

Development of Friction Stir Welding on Large Multi-Welded Components: Residual Stress,
Distortion, and Crystallographic Texture in Lap Joints of Aluminum Alloys

by

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Abstract

With the growing demand for increased battery capacity and performance, a corresponding requirement for adequate cooling of the battery cells has developed. This situation requires an efficient design of coolant channels in battery trays. The battery tray houses the cells and regulates their temperature, optimizing the distance per battery kilogram. The coolant channels in the battery tray require high-pressure die casting (HPDC) to create. However, the HPDC process is limited, and enclosed geometries for coolant flow cannot be created. Therefore, the coolant channels must be sealed with thin aluminum plates post-casting. A liquid seal is created by joining the aluminum plates to the HPDC component utilizing friction stir welding (FSW). FSW is an industry-emerging solid-state joining process by which its inherent nature overcomes many problems associated with fusion welding aluminum. These problems are due to aluminum's high-melting point oxide layer, high thermal conductivity, solidification shrinkage, and high solubility of hydrogen and other gases in the molten state, which are not an issue with FSW as the material being welded does not melt, and the brittle oxide layer breaks with the torsion of the tool intrinsically overcoming many problems related to fusion welding of aluminum. Nevertheless, the FSW welding operation leads to the development of residual stress, which compounds into a significant amount of distortion, specifically in multi-welded components, leading to expensive downstream straightening processes to restore the pre-weld geometry, which may or may not further increase residual stress. Residual stress, exceeding the stress limit of the battery tray during operation, can compromise the battery-cell housing and risk the vehicle's safety. Hence, understanding residual stress formation is vital for design, safety, service life longevity, and distortion control. Experiments utilizing neutron diffraction investigate and measure residual stress inside the battery tray, comparing the stress redistribution before and after straightening operations. The goal of the dissertation is to investigate and potentially optimize FSW in multi-welded structures to mitigate or eliminate straightening operations, enabling significant increases in production efficiencies, more robust final products, and, ultimately, a safe and environmentally friendly means of transportation.

Lay Summary

Advancements in manufacturing technologies have enhanced the durability and efficiency of components across various industries. Sectors like transportation and aerospace seek lighter, stronger structures to reduce environmental impacts and improve fuel efficiency, making superior joining techniques pivotal. Traditional welding methods, while foundational, often introduce undesirable residual stresses and distortions that can reduce component life and increase production costs. This research focuses on optimizing a novel technique called Friction Stir Welding (FSW). Unlike traditional methods, FSW joins materials without melting them, minimizing residual stresses and distortions. However, these effects have not been extensively studied in large components with multiple welds, which alter the characteristics of distortion and residual stress generation. This dissertation explores these complex characteristics to improve the reliability and quality of entire components. Enhancements in this area could extend component lifespans, streamline production, and reduce energy consumption and costs, aiding the automotive industry in achieving more sustainable manufacturing practices.

Preface

This dissertation presents the original research work conducted by the author. Several parts of the current research were developed in collaboration with individuals from Canadian and International research facilities. These individuals include Dr. Dimitry Sediako from the Faculty of Applied Science at the University of British Columbia (Canada); Dr. Glenn Byczynski and Dr. Anthony Lombardi from Nemak (Canada); Dr. Andrew Payzant from Oak Ridge National Laboratory (USA); and several student members from the High-Performance Powertrain Materials Laboratory at the University of British Columbia (Canada).

Chapter 3 of this dissertations includes versions of:

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- **N. Sabry**, J. Stroh, D. Sediako, G. Byczynski, A. Lombardi, A. Payzant, Stress characterization for friction-stir-welded electric vehicle battery trays with application of neutron diffraction, *J. Manuf. Process.* 101 (2023) 1109–1123. <https://doi.org/10.1016/j.jmapro.2023.06.035>.
- **N. Sabry**, J. Stroh, D. Sediako, Effects of the Friction Stir Welding Sliding and Sticking Mechanisms on the Microhardness, Texture, and Element Concentration, *Light Met.* 2023. (2023) 365–375. <https://doi.org/10.1007/978-3-031-22532-1>.

Additionally, chapter 4 includes future publications from work completed in collaboration with individuals from the National Research Council (Canada) and the Australian Nuclear Science and Technology Organization (Australia). These authors include Dr. Kirk Fraser and François Nadeau from the National Research Council (Canada); Dr. Anna Paradowska and Dr. Mark Reid from the Australian Nuclear Science and Technology Organization (Australia).

N. Sabry conducted the experimental work included in the papers mentioned above and prepared the original draft of the research articles. Dr. Joshua Stroh (J. Stroh) participated in experimental work and contributed to manuscripts. Dr. Dimitry Sediako (D. Sediako) secured funding and participated in experimental work, discussed results, provided equipment for experiments,

provided guidance, and reviewed and edited all manuscripts. Dr. Glenn Byczyński (G. Byczyński) and Dr. Anthony Lombardi (A. Lombardi) provided the sample material, completed data processing, discussed the results, and reviewed and edited the manuscripts. Dr. Andrew Payzant (A. Payzant) contributed to securing beam time, conducted experimental work, and reviewed the manuscripts. Dr. Kirk Fraser and François Nadeau provided the sample material, completed simulations, discussed the results, and reviewed and edited the manuscripts. Dr. Anna Paradowska and Dr. Mark Reid contributed to securing beam time and conducted experimental work.

The following copyrighted materials were utilized in this dissertation:

1. Figure 6: Grain structure evolution during 6xxx processing (Engler et al. [17])
2. Figure 7: A schematic diagram of the relative contributions to the full aging curve of the intrinsic strength, solid solution strength, and precipitation hardening (Shercliff et al. [18])
3. Figure 9: Cast Al-Si alloys (a) hypoeutectic (b) eutectic (c) hypereutectic microstructure (Warmuzek et al. [23])
4. Figure 12: Heat generation and transfer in the FSW process (Simar et al. [31])
5. Figure 13: Influence between variables with increasing spindle speed (Colligan et al. [46])
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7. Figure 15: Cross-section of a friction stir weld in a lap configuration (Aldanondo et al. [48])
8. Figure 16: (a) Flow path of surface material around a clockwise rotating pin in friction stir welding; (b) Metallographic marker experiment using transverse copper foil, illustrating internal material flow induced by pin (Seidel et al. [49])
9. Figure 17: (a) Variation of average grain size as a function of the tool rotation rate, ω ; (b) Tool traverse speed, v ; (c) Ratio, ω/v (Mishra et al. [53])
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12. Figure 20: (a) Yield strength profiles of friction stir welded AA6013-T4, compared to hardness profile from an equivalent friction stir weld; (b) Longitudinal, L, and transverse, LT, direction residual stress profile for FS welded AA6013-T4 (Lemmen et al. [58])
13. Figure 21: (a) Predominant slip systems in cubic lattices; (b) Derivation of Schmid factor for a single slip system (Schmid et al. [63]; Ennis et al. [64])
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Chapter 1: Introduction

Friction stir welding (FSW) has facilitated significant component design development of many aluminum alloys in certain sectors of the automotive industry by offering a solid-state solution that addresses many challenges posed by traditional fusion welding methods. However, residual stress, a byproduct of FSW, remains an unresolved issue, specifically in multi-welded components that significantly impact the engineering properties of components, such as fatigue life, distortion, and dimensional stability. This dissertation investigates the residual stress in battery trays manufactured in part by FSW, specifically focusing on lap weld configurations with dissimilar aluminum in multi-welded components; as much of the literature focuses on single friction stir butt welds, a gap in the understanding of how the application and proximity of welds on multi-welded components affect the distortion and resulting residual stress is present. This dissertation also investigates the residual stress in the battery trays as a consequence of post-weld straightening operations utilized to restore the pre-weld geometry by local deformations of the weld or forceful global bending of the multi-welded component. To provide initial background on the project, process, and research, the FSW operation generates heat utilizing a rotating tool pin and shoulder, inserted into the workpiece and traversed along the weld path the thermo-mechanically stirring and forging of the workpieces below melting temperature by the processes of dynamic recrystallization creates the joint. Because melting is avoided, FSW enables the joining of aluminum and dissimilar aluminum alloys, circumventing issues found in fusion welding, such as liquidation cracking, solidification cracking, and porosity. Consequently, FSW has become popular for aluminum applications in industries like automotive manufacturing (Majeed et al. [4]). For instance, Nemak, a leading provider of lightweight solutions for the global automotive industry and industry partner for this research, utilizes FSW to manufacture electric vehicle battery trays, a critical component in electric vehicles. These battery trays consist of high-pressure die cast A365 aluminum (see Figure 1) and require FSW to seal exposed cooling channels with several aluminum 6061 plates to create a multi-welded component. The high-pressure die-casting process is limited in creating internal geometries, therefore requiring FSW to seal all exposed (see Figure 2) cooling channels with several 6061 plates, creating a multi-welded component (see Figure 3).

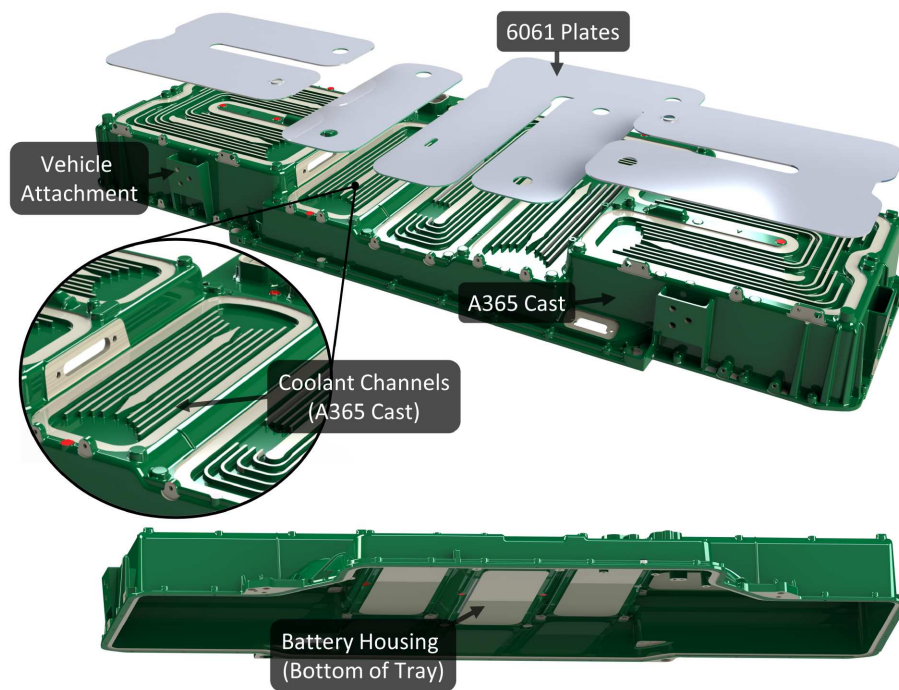


Figure 1: Battery tray structure before friction stir welding

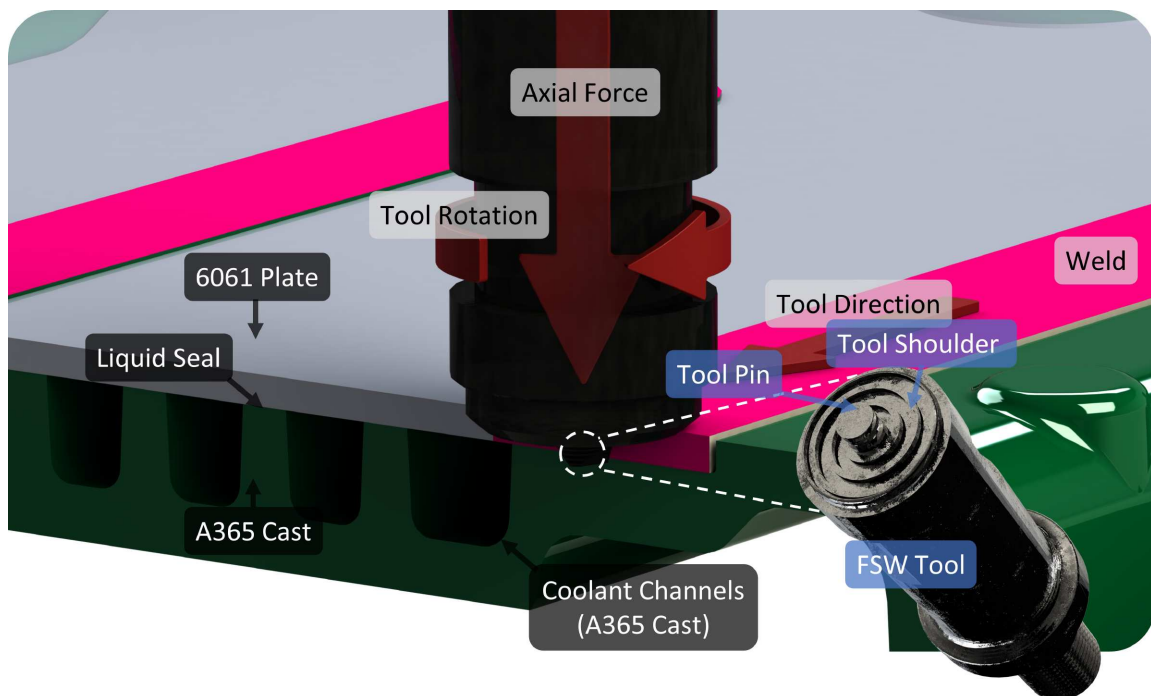


Figure 2: Friction stir welding (FSW)

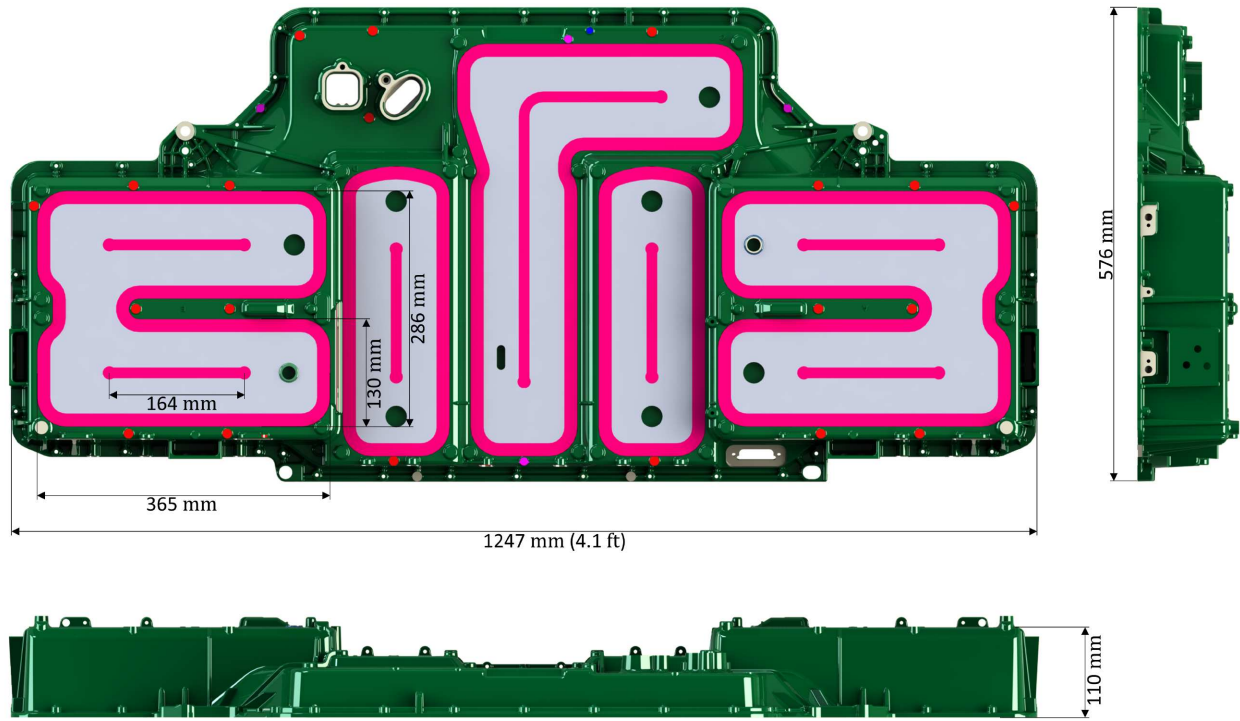


Figure 3: Friction stir weld length dimensions and overall battery tray dimensions

Nevertheless, the welding process combines two different aluminum alloys and introduces unique microstructure, temperature gradients, and non-uniform distortions that translate to residual stress in the component. Residual stress in any part considerably affects engineering properties such as fatigue life, distortion, and dimensional stability (Farhang et al. [5]). Specifically, in complex components with multiple welds, the geometry becomes especially distorted. The distortion remaining in the battery tray is reduced by applying the burnishing or coining straightening processes. The burnishing process involves a rotating indentation tool that plastically deforms the surface layer material to combat the generated tensile residual stress due to FSW. In contrast, the coining process bends the entire tray with a large die to achieve dimensional specifications. Each straightening process uniquely influences the residual stress in the battery tray. Consequently, residual stress analysis is a crucial stage in optimizing the manufacturing of the battery tray. Richards et al. [6] describe how residual stresses in FSW arise from plastic misfit strains introduced due to the steep gradients in temperatures as heat is generated from the rotating tool and distributed down the component as the tool advances. In FSW, both tensile and compressive residual stresses can be generated within the welding zone, affecting the residual stresses in other areas of the part as they attempt to compensate. Usually, maximum tensile residual stresses are on either side of the weld line, with lower residual stresses in between, forming an M-shaped residual stress profile distribution (Richards et al. [7]; Delijaicov et al. [8]; Zhang et al. [9]).

There are several ways to assess residual stress in FSW. Dawson et al. [10] measured longitudinal and transverse stresses in a butt weld configuration with similar steels using neutron diffraction. Peel et al. [11] used X-ray diffraction to measure longitudinal and transverse stresses in similar aluminum alloys. Jian et al. [12] used a common but less reliable residual stress measurement, the hole-drilling method, on similar butted aluminum. In this dissertation, the most reliable of the methods, neutron diffraction, is used to measure residual stress in the battery tray.

In any case, there have been few attempts to understand the residual stress in a lap weld configuration with dissimilar aluminum alloys, especially in multi-welded components, with insufficient comprehensive studies completed in the literature. Through the added benefit of comparing the effects of straightening processes on the redistribution of residual stress, this work aims to fill many gaps in the literature regarding these aspects. The completion of this task is highly feasible due to the support of experts at Nematik, including Dr. Byczynski, Dr. Lombardi, and Robert Hanson, who provide high-quality friction stir welded battery trays in the as-welded and straightening condition. Furthermore, the analysis is conducted utilizing world-renowned neutron facilities at Oak Ridge National Laboratory and the Australian Nuclear Science and Technology Organisation. Additionally, the supervisor of this dissertation, Dr. Sediako, brings extensive expertise in measuring residual stress in many manufacturing processes (Smith et al. [13]; Lombardi et al. [14]; Sediako et al. [15]; Sediako et al. [16]).

Therefore, this research will significantly advance the manufacturing field by providing a deeper understanding of the factors influencing distortion and residual stress in FSW, particularly for multi-welded components. By elucidating the effects of weld sequence, proximity, and welding speeds at a constant speed ratio on distortion and residual stress, the research will enable engineers to optimize the FSW process for a variety of applications, including lightweight automotive components, aerospace structures, and other advanced manufacturing industries. Furthermore, the findings of this research have the potential to improve the fatigue life, dimensional stability, and overall performance of FSW components, leading to enhanced safety, reliability, and longevity in various engineering applications. Ultimately, this research will contribute to the development of more efficient, sustainable, and cost-effective manufacturing processes, driving innovation and competitiveness in the global engineering landscape.

1.1 Scope of Research

This dissertation investigates the residual stress and distortion characteristics of friction-stir-welded (FSW) hybrid electric battery trays, focusing on multi-welded components. It examines the effects of various welding parameters, including rotational speed, traverse speed, and weld sequence, on the formation and distribution of residual stresses in the welded battery trays. This research also explores the impact of post-weld straightening techniques, such as burnishing and coining, on residual stress redistribution and distortion control.

The study utilizes neutron diffraction, a reliable method for measuring internal stresses, to evaluate the residual stress profiles in battery trays both before and after the application of straightening processes. By comparing these profiles, the research aims to identify the optimal parameters and techniques to minimize residual stresses and distortion, thus enhancing the structural integrity and performance of the battery trays.

Furthermore, this dissertation advances the understanding of the microstructural changes induced by FSW in dissimilar aluminum alloys (6061 and A365), providing insights into how these changes affect the mechanical properties and residual stress distribution. The research leverages Smoothed Particle Hydrodynamics (SPH) simulations to predict temperature distributions and material flow during the welding process, complementing the experimental findings and aiding in the optimization of FSW parameters.

The knowledge gained from this research will contribute to the development of more efficient and robust manufacturing processes for hybrid electric vehicle battery trays, leading to improved safety, reliability, and longevity of the components. Ultimately, this study aims to provide a comprehensive framework for mitigating residual stresses and distortion in FSW applications, promoting sustainable and high-quality production practices in the automotive industry.

Overall, the objectives of this dissertation can be listed as follows:

- Investigate and assess the distortion, residual stress, and texture of the as-welded and straightened battery trays to understand their behavior and differences.
- Determine the impact of weld proximity on residual stresses in the as-welded battery tray.

- Characterize the texture of the as-welded battery tray and explore its relationship with residual stress.
- Investigate texture variations between different welds on the battery tray and evaluate their influence on mechanical properties.
- Compare the distortion among as-welded, burnished, and coined straightened battery trays to identify the effectiveness of each straightening method.
- Analyze the redistribution of residual stress after each straightening operation to determine if stresses become more favorable (less tensile and more compressive).
- Investigate how varying speed parameters, given a constant speed ratio, affect the texture and residual stress in FSW components.
- Determine the specific welding speed that results in the desired texture and reduced residual stress in FSW components.
- Analyze how the sequence of welds in multi-welded components influences distortion and residual stress.
- Identify welding sequences that result in reduced distortion and residual stress in multi-welded FSW components.

1.2 Structure of Dissertation

This proposal has been structured as follows:

Chapter 1: Introduction: This chapter outlines the significance of this dissertation for both the automobile industry and the environment, providing a general overview of the research. It also defines the scope of the study and details the structure of the dissertation.

Chapter 2: Literature Review and Background: In this chapter, the literature and explanations relevant to the aluminum system, fundamentals of friction stir welding, and factors affecting the welding process are reviewed. It identifies the knowledge gaps and sets the stage for the entire project by highlighting their influence on mechanical properties, microstructure, and residual stress.

Chapter 3: Stress, Texture, Distortion, and Materials Characterization on the Friction Stir Weld Utilized on EV Battery Tray: In this chapter, the residual stress, microstructure, and mechanical

properties of the dissimilar aluminum friction stir weld lap joints on an electric vehicle battery tray are characterized to understand the many factors that influence distortion and residual stress generation.

Chapter 4: Stress, Texture, Distortion, and Simulations on the Friction Stir Weld Utilized on Controlled Plate Geometry: This chapter addresses the need for optimization in distortion control for multi-welded components, aiming to enhance the manufacturing industry's understanding of FSW. The goal is to significantly increase production efficiencies, produce more robust final products, and ultimately provide a safe and environmentally friendly means of transportation. The proposed approach involves comparing in-house welded standard coupons subjected to various monitored changes in welding speed and weld order. Each coupon is then analyzed using neutron diffraction, texture analysis by electron backscatter diffraction, and distortion analysis.

Chapter 5: Conclusion: This chapter summarizes the findings from Chapters 3 and 4. The chapter also outlines the findings of the project, providing a comprehensive understanding of the significant aspects that drive the research forward.

Figure 4 outlines the general flow and connection of the studies completed and planned for this dissertation.

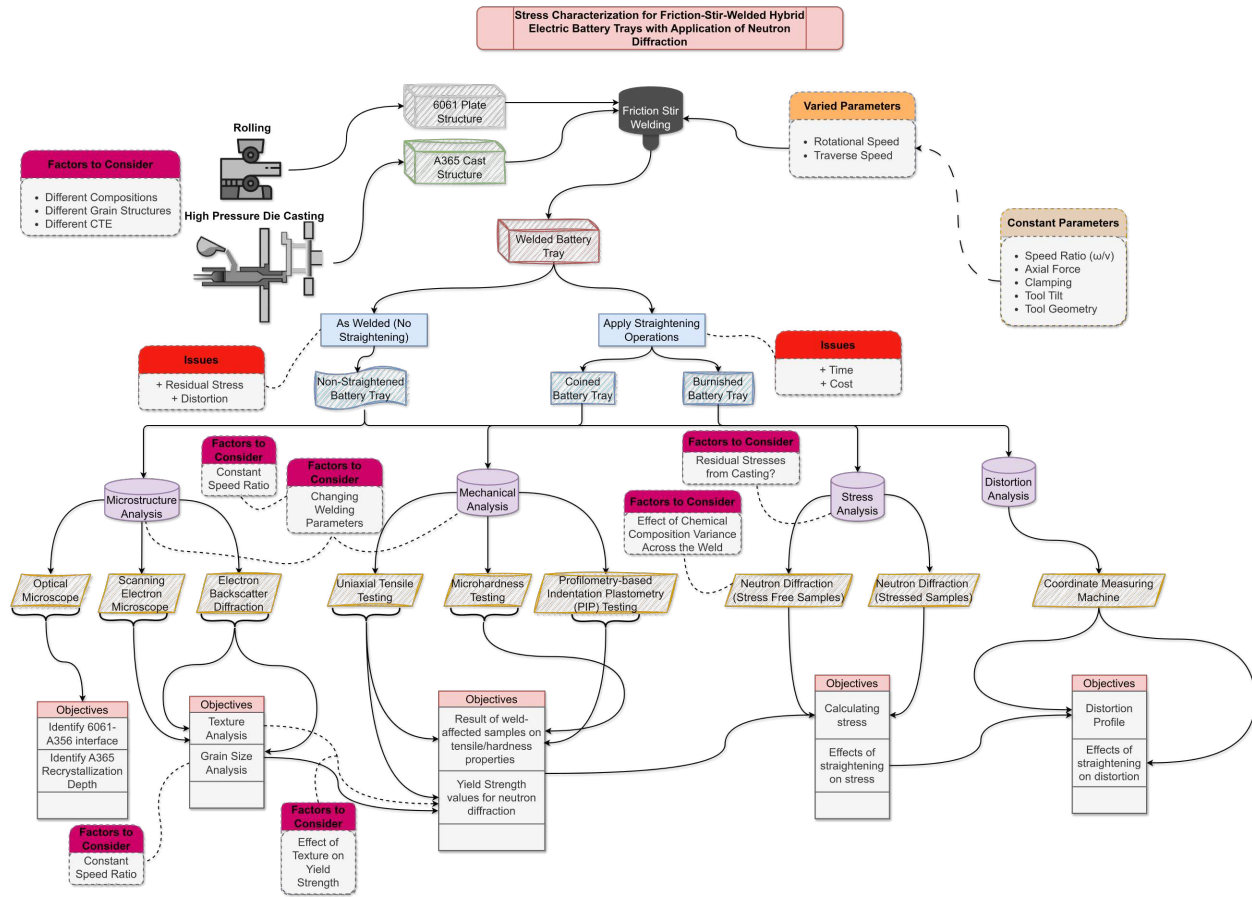


Figure 4: Flow chart displaying the proposed analysis to be carried out for this dissertation

Chapter 2: Literature Review and Background

This chapter aims to review the relevant literature necessary for this dissertation. The initial sections cover high-strength aluminum alloys and the evolving friction stir welding (FSW) technique used to join these alloys. The chapter then explores the development of welding-induced residual stress, particularly in the context of FSW for high-strength aluminum alloys, and how this stress leads to component distortion. Next, existing methods for minimizing residual stress are examined, followed by a discussion of the techniques for measuring residual stress. The chapter concludes with a review of texture measurement methods. Neutron diffraction techniques were employed in this dissertation to determine residual stress across multiple components, including full-sized industrial elements and geometrically simplified structures, to understand the impact of various welding parameters. These diffraction techniques and their associated challenges are discussed in the final section.

2.1 Aluminum Alloys

Aluminum alloys typically consist of 90 to 96% aluminum (Al), with the remaining portion made up of alloying elements such as silicon (Si), magnesium (Mg), manganese (Mn), copper (Cu), or zinc (Zn). The choice of alloying elements depends on the desired manufacturing or performance characteristics and often includes several minor elements alongside the major ones. Aluminum alloys are classified as either cast or wrought alloys. Wrought products include rolled plates and extrusions. Cast aluminum alloys are formed when molten aluminum is poured into a mold or die, taking the shape of the mold as it cools and solidifies. Cast alloys generally have a lower melting point and higher fluidity, but they typically exhibit lower tensile strength and reduced malleability compared to wrought aluminum. These differences in mechanical properties result from defects introduced during the casting process and the inherently larger grains within their microstructure. The Al-Si system is crucial in casting due to the elevated silicon levels (4.0–13%), which provide excellent casting characteristics, such as high fluidity.

Given these material characteristics, welding aluminum, particularly when joining wrought aluminum to cast aluminum, presents several challenges. Firstly, aluminum naturally forms a thin layer of aluminum oxide on its surface when exposed to air. This oxide layer has a much higher melting point than the aluminum beneath it, interfering with the fusion process. Additionally,

aluminum's high thermal conductivity and low melting point cause heat to spread quickly through the material, making it difficult to localize the heat input required for fusion welding and increasing the risk of burn-through. Moreover, aluminum, especially high-strength alloys, is susceptible to hot cracking, a defect that occurs during cooling and solidification. The silicon content in aluminum alloys also affects weldability, with a silicon content of 0.8-1.5% deteriorating weldability due to increased fluidity and the formation of refractory silicon oxides, which can lead to hot cracks. FSW offers several advantages over fusion welding for joining aluminum. As a solid-state welding process, FSW does not melt the materials, eliminating issues such as hot cracking and the formation of brittle intermetallic compounds. The frictional heat and mechanical stirring action of FSW effectively break up and disperse the aluminum oxide layer, allowing for better bonding. FSW generates less heat than fusion welding, resulting in reduced thermal expansion and contraction, which in turn reduces residual stress and distortion in the welded joint. However, differences in thermal expansion coefficients between dissimilar materials, like wrought and cast aluminum, can still lead to residual stress. The FSW process can also handle differences in silicon content between wrought and cast aluminum better than fusion welding, as the stirring action helps to homogenize the silicon distribution and reduce the risk of hot cracking.

2.1.1 Processing of 6xxx Aluminum Alloys

The 6xxx series alloys are versatile aluminum alloys primarily composed of aluminum, silicon (Si), and magnesium (Mg). These elements combine to form magnesium silicide (Mg_2Si), which imparts a range of beneficial properties to the alloys. 6xxx series alloys have a good balance of properties, offering a combination of strength, corrosion resistance, weldability, machinability, and surface finish. This makes them suitable for a wide variety of applications across different industries. In transportation, these alloys are used for structural applications due to their robust mechanical properties and corrosion resistance. In architectural projects, including bridge construction and repair, their resistance to atmospheric corrosion makes them ideal for structures exposed to the elements. The 6xxx series is also widely utilized in consumer products, piping and tubing, and electrical components, thanks to their good machinability and surface finish. In construction, the strength, finish, and ability to receive protective coatings make these alloys suitable for various structural uses. Their extrudability, heat treatment capability, and exceptional corrosion resistance further enhance their appeal for diverse applications.

To evaluate the mechanical properties of welded high-strength aluminum alloys, it is important to understand the primary strengthening mechanisms and the impact of elevated temperatures, such as those experienced during FSW, on the material's properties. This section provides an overview of the strengthening mechanisms in high-strength aluminum alloys. Aluminum's primary advantage over steel is its lower density, making it suitable for lightweight applications. Pure aluminum, however, lacks adequate strength, necessitating alloying with various elements and several processing steps to enhance its properties. These processing stages significantly influence the mechanical properties of 6xxx series aluminum alloys; properties such as the alloy's microstructure, grain size and distribution, volume-fraction of precipitates, and crystallographic orientation are dictated by the alloy's composition and processing steps.

The strength of 6xxx series aluminum alloys primarily arises from precipitation or age-hardening mechanisms. For an alloy to be suitable for precipitation hardening, there must be a significant decrease in the solid solubility of the alloying elements as the temperature decreases. The standard procedure for precipitation-hardening aluminum alloys to enhance their mechanical properties, such as maximizing material strength, involves several key steps:

1. Solution heating above the solidus temperature within the single-phase region to dissolve all alloying elements.
2. Quenching the material to form a supersaturated solid solution of the alloying elements within the aluminum matrix.
3. Aging by thermal treatment to decompose the supersaturated solid solution and form a fine dispersion of precipitates.

These strengthening steps are carried out at the final stage of material processing (see Figure 5). While the grain structure of aluminum does not undergo significant physical changes after solution heating (see Figure 6), quenching and aging play a critical role in dispersing precipitates throughout the material, significantly boosting its strength. These final steps are referred to as “tempering,” and different tempers have varying impacts on strength.

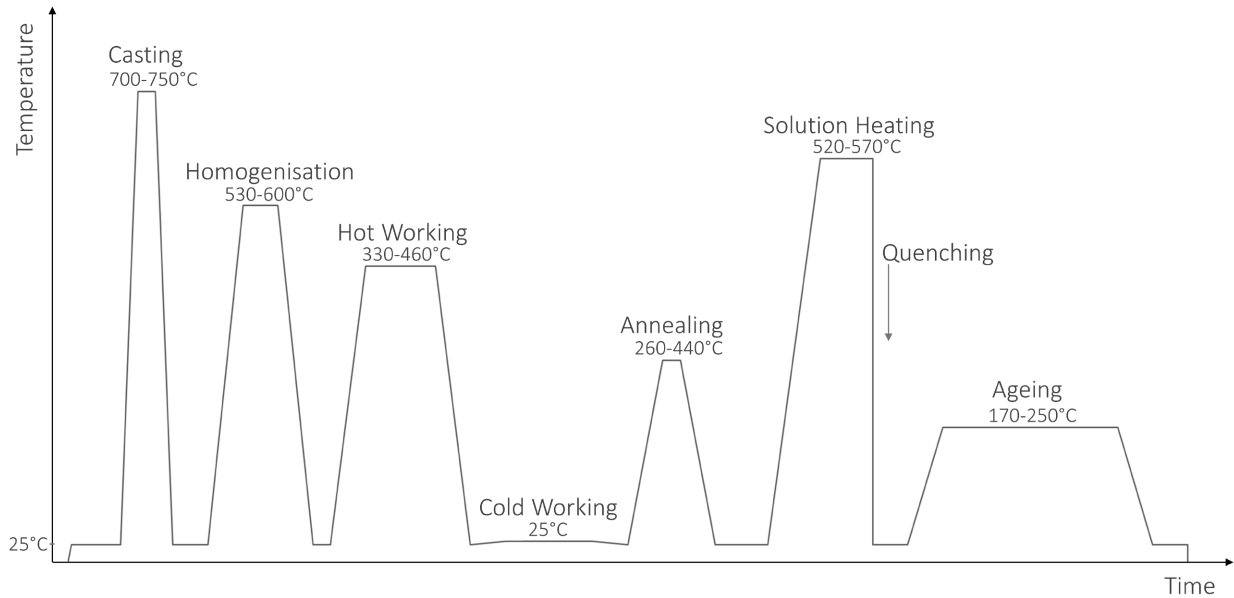


Figure 5: Schematic Processing route for 6xxx series Al alloys

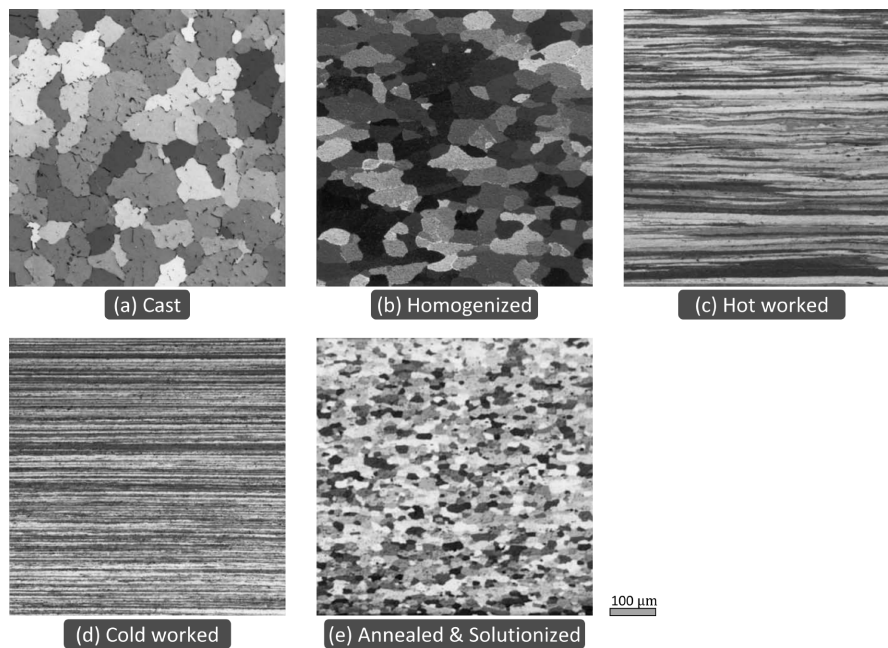


Figure 6: Grain structure evolution during 6xxx processing (Engler et al. [17])

As Shercliff et al. [18] have outlined, these precipitates are the most significant contributors to the material's yield strength (see Figure 7). The precipitates induce lattice distortions, which impede dislocation motion, thereby increasing the yield strength and hardness of the material. At the beginning of the “net aging curve” in Figure 7, under-aged conditions are evident, where peak strength is not achieved because dislocations can disrupt smaller precipitates. However, as aging progresses, the precipitates grow, making it harder for dislocations to break them, resulting in

increased material strength. The peak strength is achieved in the peak-aged condition, designated as the T6 temper. However, as the precipitates continue to develop, dislocations will begin to bypass them by bowing around, leading to a decrease in the material's strength, known as over-aging or the T8 temper. Temperature sensitivity plays a crucial role in the aging process, where even minor variations can result in significant changes in the precipitation microstructure and the material's mechanical properties. In the context of FSW of 6xxx aluminum alloys, the material undergoes a localized thermal cycle near the weld line, which mirrors the conditions of precipitation-hardening in terms of magnitude and duration. Due to the sensitivity of precipitate formation and structure to both aging temperature and time, this localized thermal cycle can lead to considerable alterations in the precipitate microstructure. These changes manifest as major variations in the distribution, size, and density of precipitates and, consequently, the mechanical properties across the weld.

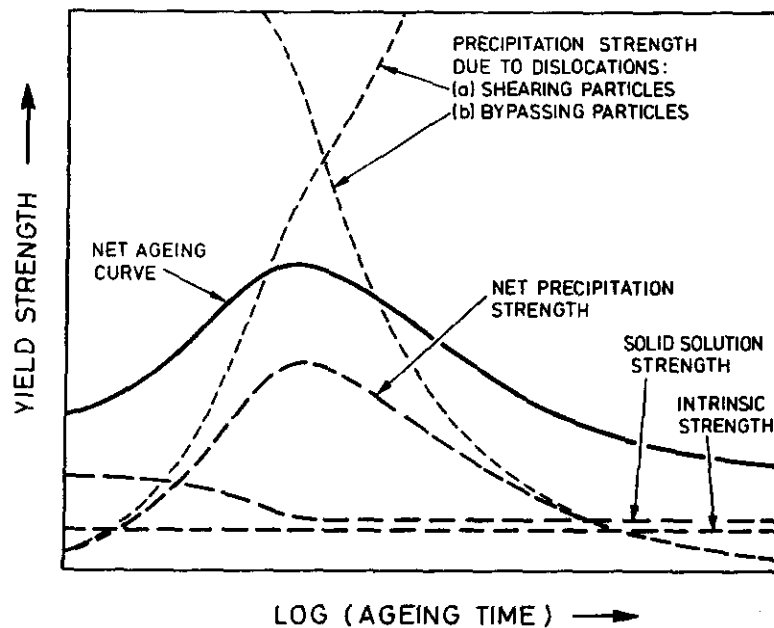


Figure 7: A schematic diagram of the relative contributions to the full aging curve of the intrinsic strength, solid solution strength, and precipitation hardening (Shercliff et al. [18])

The thermal-mechanical cycle imposed by FSW impacts the mechanical properties, particularly yield strength, which in turn affects the distribution of residual stress, as discussed in section “2.3 Residual Stress in Friction-Stir-Welding”. Therefore, to provide context for the following sections, it is essential to introduce the general precipitation sequences and microstructures of this material.

2.1.2 3xx.x Aluminum Alloys and Dissimilarity Considerations

The 3xx.x series casting alloys, commonly known as the 3xx series, are aluminum alloys primarily composed of aluminum and silicon, with varying amounts of magnesium and other elements. These alloys are designed for casting processes, offering excellent castability and a range of mechanical properties suitable for various applications. The presence of silicon improves fluidity and reduces shrinkage during casting, making these alloys ideal for complex shapes and thin-walled components. The addition of magnesium further enhances their strength and hardness. These properties make the 3xx series alloys particularly useful for manufacturing engine blocks, cylinder heads, and various structural components where high performance and reliability are important.

3xx.x series alloys are widely used in the automotive and aerospace industries due to their good combination of strength, ductility, and corrosion resistance. The 3xx.x cast alloy, specifically A365.0 (or A365), is employed in the production of complex geometries such as battery trays (see Figure 1). These trays feature intricate and thin-walled structures that are challenging to achieve with traditional gravity-casting methods due to insufficient pressure. High-pressure die casting (HPDC) addresses this limitation by injecting the liquid aluminum into a negative geometry die under high pressure (40-160 MPa) and maintaining die temperatures between 120°C to 280°C (see Figure 8). This method ensures high metal flow and rapid solidification, leading to near-net shapes with excellent surface finish and dimensional accuracy, minimizing post-casting machining and finishing requirements.

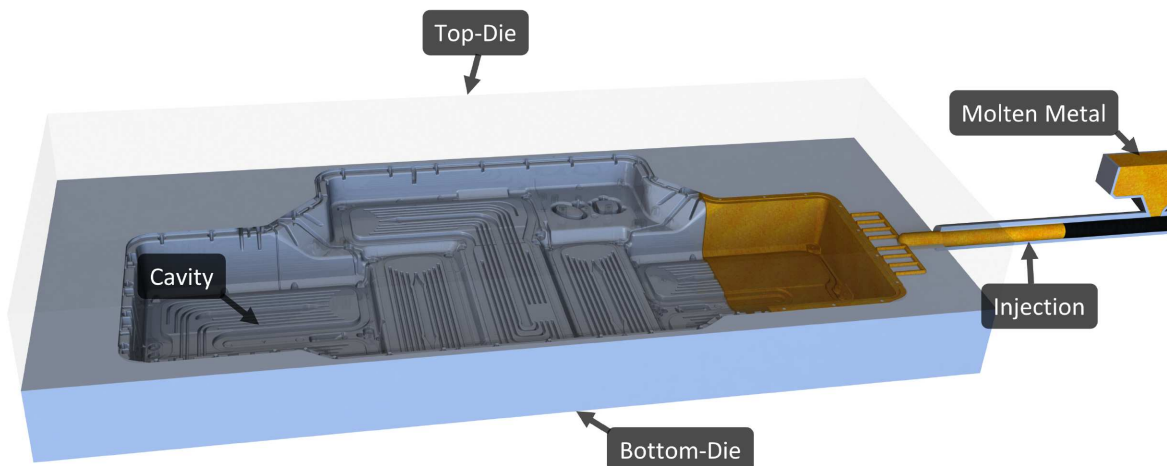


Figure 8: Schematic of high-pressure die-casting processes for the battery tray

The tensile strength and mechanical properties of cast aluminum alloys like A365 are significantly influenced by their microstructure, which is determined by factors such as silicon content and grain size. The silicon content in A365 ranges from 9.5 to 12.6 wt.%, leading to a hypoeutectic structure comprising approximately 73% α -Al and 27% eutectic structure (Figure 9 and Figure 10). This hypoeutectic structure is beneficial because it enhances fluidity during casting, ensuring better filling of the die cavity and reducing the likelihood of defects.

The critical distinction between FSW of similar and dissimilar alloys lies in the property discontinuity across the joint interfaces, potentially leading to significant impacts on material flow patterns during the stirring process. The considerable difference in plasticized material flow patterns and dynamic recrystallization within the stirred regions of dissimilar compared to similar alloys is well-known and discussed by Sharma et al. [19], DebRoy et al. [20], Tavares et al. [21] and Peel et al. [22]. Specifically, the 6061 (wrought) and A365 (cast) alloys are dissimilar for several reasons, including differences in post-processing, the presence and density of precipitates, and differences in grain sizes that locally change the stiffness and other properties in wrought alloys.

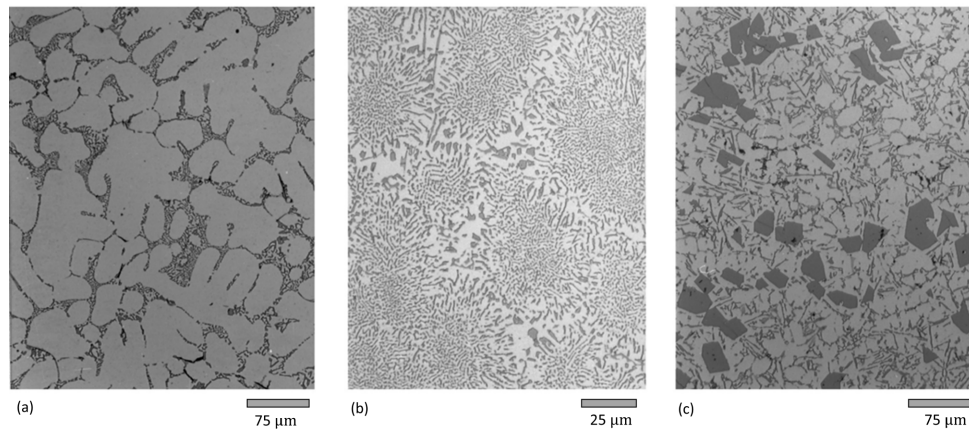


Figure 9: Cast Al-Si alloys (a) hypoeutectic (b) eutectic (c) hypereutectic microstructure (Warmuzek et al. [23])

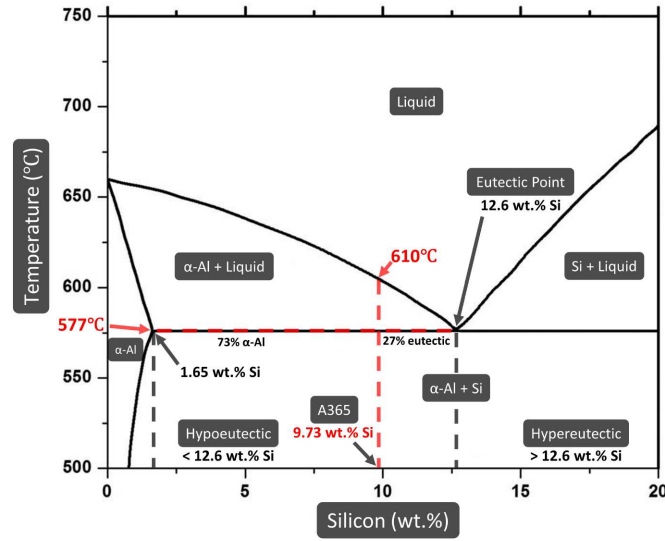


Figure 10: The aluminum-silicon phase diagram

The compositional differences between A365 and 6061 alloys create critical phases that influence strength and other properties specific to the alloy and its processing. One such property vital for casting the A365 alloy is fluidity when the molten material is forced throughout the die-casting in the HPDC operation (Wang et al. [24]). Fluidity increases with additional silicon, and a significant difference in silicon content between A365 and 6061 can be seen in Table 1. The increased silicon content in A365 enhances fluidity, which is crucial for achieving complex geometries in HPDC, but it also reduces ductility, making high-silicon cast alloys more susceptible to brittle fracture (Birol et al. [25]). Conversely, wrought alloys like 6061, with significantly lower silicon content, exhibit higher strength and ductility due to differences in post-processing, chemical composition, and microstructure.

Table 1: 6061 and A365 chemical composition (shown in wt.%) (Birol et al. [25]; Ekambaram et al. [26]; Jeong et al. [27])

	Si	Mg	Cu	Fe	Zn	Mn	Cr	Ti	Ni	Sr	Al
6061	0.70	0.90	0.30	0.60	0.20	0.05	0.3	0.10	0.05	**	Balance
A365	9.73	0.17	0.004	0.11	**	0.560	**	0.07	**	0.02	Balance

One of the critical factors in welding 6061 to A365 is the difference in their thermal expansion coefficients (A365 at $\sim 21 \mu\text{m/m-K}$ and 6061 at $\sim 24 \mu\text{m/m-K}$, Hidnert et al. [28]). These differences can generate significant residual stresses during the FSW process, as each material expands and contracts at different rates when subjected to heat. If not properly managed, these

residual stresses can lead to distortion, compromising the structural integrity of the welded component.

The 6061 alloy typically has a uniform and fine-grained microstructure, while the A365 cast alloy exhibits coarser grains and varied phases. These microstructural differences affect the materials' responses to thermal cycles during welding, influencing the formation of residual stresses and potential distortion. The mechanical properties of 6061 and A365, such as yield strength, ductility, and hardness, also differ. These variations can lead to differential deformation during welding, potentially compromising the integrity and performance of the welded joint. The quality of the weld and the likelihood of defects such as voids, cracks, and incomplete fusion can vary between 6061 and A365 materials. These defects, influenced by the welding process, can significantly impact the overall quality of the weld; therefore, steps are taken in this dissertation to identify welding parameters in which defects do not occur and verify that fact with non-destructive testing as completed in section “4.1.1.1 Weld Layout and Quality Assurance of Single Welded Plates”.

Optimizing welding parameters, such as tool rotation speed, traverse speed, and tool design, is crucial to minimize residual stress and distortion while ensuring a strong, defect-free weld. The Heat Affected Zone (HAZ) characteristics of 6061 and A365 alloys differ due to their distinct thermal conductivities and microstructures. Understanding these differences is important for accurately understanding the measured residual stresses in this dissertation and for predicting and managing deformation during or after welding.

2.1.3 Section Summary

In summary, the successful welding of 6061 aluminum alloy to A365 cast alloy requires careful consideration of their distinct microstructures, thermal properties, and mechanical behaviors. By optimizing welding parameters and understanding the inherent differences between these materials, it is possible to achieve high-quality welds with minimal residual stress and distortion.

Research on the FSW of dissimilar materials, such as aluminum and magnesium alloys, underscores the complexity of achieving a uniform weld. The formation of complex, intercalated vortex and flow patterns in dissimilar FSW systems is facilitated by dynamic recrystallization, leading to significant hardness variations and residual stresses across the weld zone (Murr et al. [29]). This complexity necessitates being aware of the differences in properties and how they might

affect the analysis of distortion and residual stress. Murr et al. [29] highlight that residual micro-indentation hardness measured across the weld face provides comparative performance signatures, showing how hardness fluctuations or complex spikes occur in dissimilar systems, often skewed from the weld centerline when the tool rotation direction changes. Although the referenced studies mainly consider butt welds, many insights remain relevant to lap welds, such as the 6061-on-top and A365-on-bottom configurations used in this research.

Further research is essential for a comprehensive understanding of the effects of dissimilar alloys on residual stresses in FSW. Although the material system for the battery tray is predetermined by industrial requirements, awareness of these effects is critical. The studies by DebRoy et al. [20], Murr et al. [29], and Shah et al. [30] suggest that the impact of dissimilar alloys on residual stress outcomes depends on the specific materials being joined, the employed welding parameters, and the material location within the joint. This understanding is crucial for developing robust FSW practices that ensure high-quality, reliable welds while minimizing distortion and residual stress in industrial applications.

In conclusion, while significant progress has been made in understanding the challenges and optimizing the FSW of dissimilar materials, ongoing awareness and consideration of these factors are necessary. By addressing the unique challenges presented by dissimilar alloy welding, researchers can continue to enhance the performance and reliability of FSW joints in various applications.

2.2 Friction-Stir-Welding

A joining process is required to seal the inner coolant channels of the battery tray (see Figure 1) created by the casting processes. Because the channels transport liquid coolant, a liquid-tight seal is required. A gasket and fastener system could provide a liquid seal; however, this would require intensive labor, add more weight to the battery tray, and increase production time. Fusion welding is an alternative joining approach, but it presents several challenges when applied to aluminum. Firstly, the melting point of aluminum (around 660°C) is significantly lower than that of, for example, steel (around 1538°C). This lower melting point makes aluminum more prone to warping and distortion during the welding process due to the intense heat required for fusion welding. In contrast, steel's higher melting point provides greater stability, reducing the likelihood of distortion. Secondly, aluminum forms an oxide layer when exposed to air. This oxide layer can

hinder proper fusion during welding, requiring special techniques and equipment to remove it. Steel, on the other hand, does not form a similar oxide layer, simplifying the welding process. Thirdly, aluminum has a higher thermal conductivity than steel. This characteristic causes heat to spread more quickly through aluminum, making it more difficult to control the heat input during welding. The rapid dissipation of heat can lead to inadequate weld penetration, as cold areas of the aluminum continuously draw heat away from the welding zone. Additionally, hydrogen is very soluble in liquid aluminum. As the filler material and aluminum base metal become liquid during the welding process, they absorb hydrogen and can hold it in solution. Once the molten material starts to solidify, it can't hold the hydrogen in a homogeneous mixture anymore. The hydrogen forms bubbles that become trapped in the metal, leading to porosity. Porosity not only weakens the joint but also compromises the essential liquid seal required for the battery tray. This issue is less prevalent in steel welding, where hydrogen solubility is lower. Furthermore, welding techniques for steel, such as metal inert gas and tungsten inert gas welding, are relatively easy to learn and yield consistent results. In contrast, welding aluminum often requires more specialized techniques and careful control to achieve quality welds. Lastly, fusion welding dissimilar materials, such as aluminum and other metals, is problematic due to the different melting points of the materials. The varying melting temperatures can cause grain boundaries to overheat, leading to the evaporation or burning of alloying elements in the material with a lower melting point.

In comparison, solid-state joining techniques such as FSW can successfully prevent problems associated with melting and solidification inherent to fusion welding aluminum. FSW is a solid-state welding process that requires no melting of the material. Therefore, solid-state welding negates hydrogen absorption, reducing the risk of porosity. It also minimizes residual stress by maintaining lower temperature differences and allows full-contact bonds between dissimilar materials with different melting temperatures. Additionally, FSW mechanically breaks the oxide layer and disperses it around the weld, eliminating its impact on the welding process. These advantages make FSW a superior choice for welding aluminum components, ensuring a robust and liquid-tight seal for the battery tray.

2.2.1 Operating Principles of FSW

The FSW process involves three distinct steps, as shown in Figure 11. Initially, both overlapping components are securely clamped to the backing plate to counteract the welding downforce. The

process begins with a rotating FSW tool, spinning at high RPM and plunging into the workpiece with significant axial force. This action generates heat through friction between the workpiece material and the tool, causing the material to plasticize near and around the tool. The tool is held in place until sufficient plasticization occurs. In the next step, the tool traverses along the joint line at the desired plunge depth. During this traversal, the plasticized material is confined between the solid material outside the heated zone, forcing it to flow around the pin from the retreating side to the surrounding solid material. This action fills in the hollow space left by the advancing tool. Vertically, the plasticized material is restricted between the rotating shoulder and the solid material below the heated zone, creating a case for the weld to form a joint. Upon reaching the end of the weld path, the tool is withdrawn from the material, leaving an exit hole, as no material moves to fill the void. FSW is an asymmetric process, with the two sides of the weld experiencing different conditions. On the advancing side, where the tool rotation and translation move in the same direction, the movement interaction is greater. Conversely, on the retreating side, the tool rotation and translation are in opposite directions, causing less movement interaction compared to the advancing side. Additionally, the front of the tool is referred to as the leading edge, while the back is known as the trailing edge.

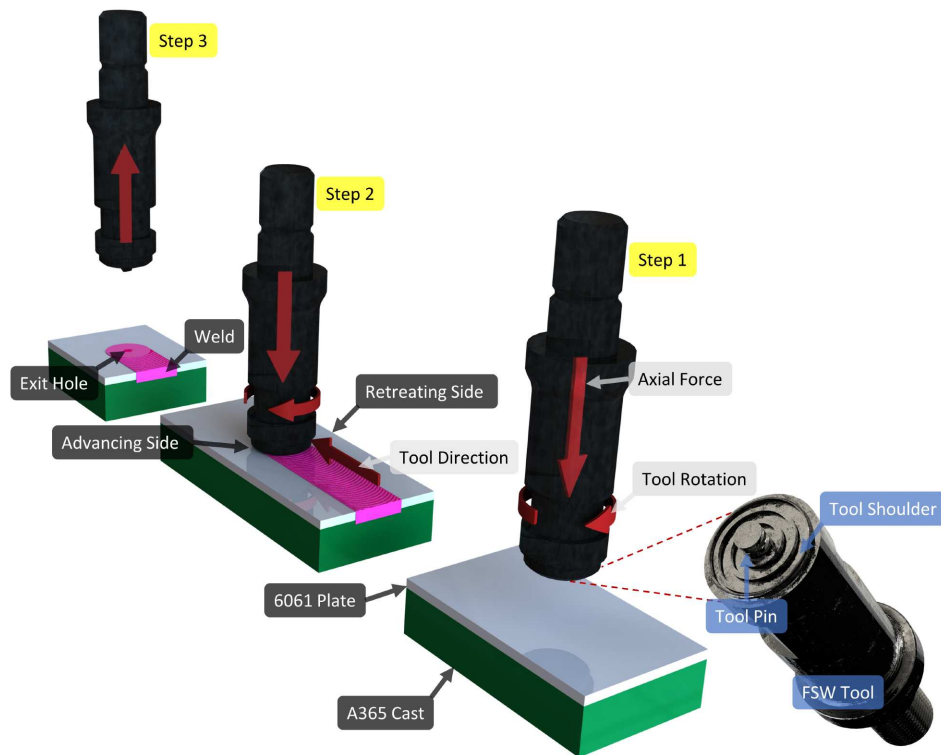


Figure 11: General friction stir welding processing steps

The detailed FSW process involves the following steps:

1. **Plunging:** The rotating tool is lowered until the pin penetrates the material and the tool shoulder contacts the plate surface, applying a preset downforce. Friction at the tool shoulder-material interface generates near-melting point temperatures, softening the metal in this area. Lower temperatures and less softened material are found around the tool pin, closer to the weld root, and lateral to the FSW tool.
2. **Traversing:** After a stationary period called the dwell, which heats and softens the surrounding material, the rotating tool traverses along the joint line. This action heats, softens, and stirs the material at the interface. The tool's shoulder and pin draw in the softened metal in front of the tool, stir it horizontally and vertically, and then forge it behind the tool to form a coherent weld upon cooling.
3. **Tool Withdrawal:** Upon reaching the end of the weld path, the tool is extracted from the material, leaving an exit hole. Typically, both the dwell point and the exit hole are removed from the welded structure.

2.2.2 FSW Heat Generation and Simulation Considerations

As the tool rotates, the friction between the tool and the aluminum generates heat, which softens the aluminum without melting it. Additionally, the rotating pin and shoulder apply pressure, causing the aluminum to plastically deform around the tool. This plastic deformation adds further heat due to internal friction within the aluminum. The tool is then moved along the joint line, continuing to generate heat and causing the aluminum to flow and mix along the joint line. The heat generated by friction and plastic deformation is concentrated around the tool but spreads into the surrounding material (see Figure 12). However, heat generation during FSW is not uniform around the entire tool. The interface between the tool shoulder and workpiece generates nearly 70% of the tool heat during the operation, while the tool pin and tool side make up the remainder (Sharghi et al. [32]). Additionally, since the tool is constantly rotating, the temperature distribution is non-uniform. Wang et al. [33] measured and simulated this with Finite Element Method (FEM), indicating that the advancing side of the tool has a maximum temperature compared to the retreating side. However, this is highly dependent on the processing parameters and material used,

with many studies measuring that the maximum temperature is neither on the retreating nor advancing side but on the trailing side of the tool (Silva-Magalhães et al. [34]).

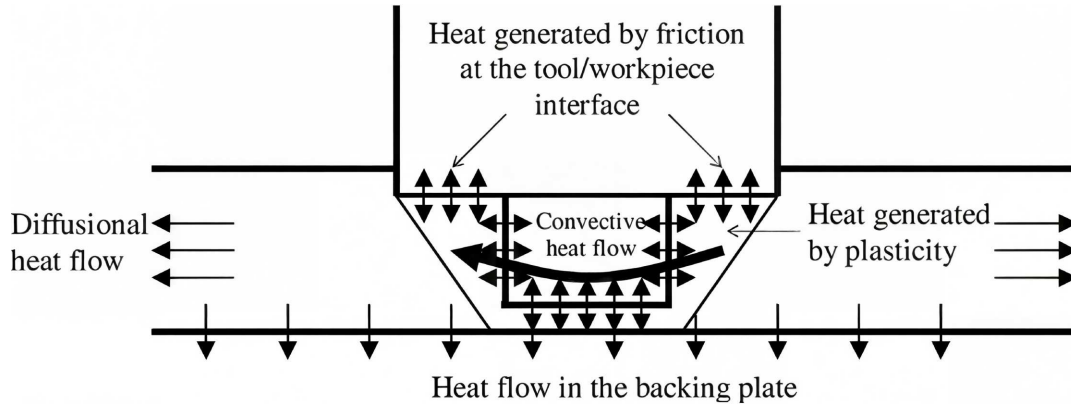


Figure 12: Heat generation and transfer in the FSW process (Simar et al. [31])

However, in conjunction with the consideration of dependencies on processing parameters, it's essential to understand that FEM-based models are meshed simulations. FSW is a complex joining process that involves multiple intertwined physical phenomena such as friction, inelastic heat generation, heat conduction, and high plastic deformations. Traditional numerical FSW models based on grid methods like FEM can predict and estimate the flow field and temperature distribution around the tool; however, they do not provide information about the interface between materials and the final distribution of welded materials. On the other hand, Smoothed Particle Hydrodynamics (SPH), being a mesh-free method, can simulate the dynamics of interfaces, large material deformations, and the material's strain and temperature history without employing complex tracking schemes. This framework makes SPH more suitable for simulating FSW, which involves large deformations and requires tracking of the material interface. Fraser et al. [35] have demonstrated the effectiveness of SPH in their work, utilizing SPHriction-3D to predict temperature, process forces/torques, internal and surface defects, tool wear, and residual stresses. Fraser et al. [35] highlight that the SPH method allows for a better understanding of the exact placement of the most heated material.

The SPH simulation method is utilized in this dissertation to better understand the exact placement of the most heated material, whether it is consistent between welding parameters for a specific material subset, and to connect this to experimentally obtained residual stress measurements as discussed in the section, "4.1.3 Smoothed Particle Hydrodynamics Simulations". Understanding the position of the maximally heated material is fundamental for analyzing how asymmetric

temperature and thermal distribution affect residual stresses along the welding line and perpendicular to it.

2.2.3 FSW Parameters

When evaluating FSW, several vital parameters must be considered: the tool's rotational speed (RPM), the traverse speed of the tool along the weld line (mm/min), and the axial force (kN). The direction of rotation, either clockwise or counterclockwise, determines the advancing side with respect to the tool traverse vector. Furthermore, factors such as the geometry of the structure, the material being welded, the tool's tilt angle, the degree of clamping and its release schedule, and the weld sequence or layout are all important and can significantly impact the outcome of the weld. Comprehensive interpretations of these variables are outside the purview of this section and are available in other sources (Threadgill et al. [36]; Shah et al. [30]), where relevant parameters are discussed briefly in certain experimental procedure sections before data analysis. Although substantial investigations and reviews have been undertaken on how tool rotational speed, traverse speed, and axial force influence the weld characteristics (Prabhakar et al. [37]; El-Sayed et al. [38]; Peel et al. [39]), the interactions among these and other variables are complex, and the effects on many measurements, such as distortion and residual stress, are not wholly comprehended. The ensuing sections will offer a concise summary of the existing knowledge concerning the impacts of rotational speed, travel speed, and axial force.

Arbegast's equation is widely referenced in FSW literature for its empirical representation of the peak temperature during welding. This equation demonstrates how changes in welding parameters affect temperature. Schwartz et al. [40], Geng et al. [41], Mishra et al. [42], et al. Sedmak et al. [43] and Golezani et al. [44] have utilized this equation to model thermal behavior in FSW. Arbogast et al. [45] represent the peak temperature empirically in Equation 1:

$$T = K \left[\frac{\omega^2}{v \cdot 10^4} \right]^\alpha \cdot T_m \quad \text{Equation 1}$$

The variables listed in Equation 1 are: T is the maximum temperature during welding ($^{\circ}\text{C}$), T_m is the melting temperature of the material ($^{\circ}\text{C}$), v is the tool traverse speed, ω is the rotational tool speed, and α (~ 0.005) and K (~ 0.7) are constants. The ratio of ω^2/v in Equation 1 demonstrates that increasing the rotational speed with constant traverse speed or decreasing the traverse speed

with constant rotational speed leads to higher welding temperatures. Additionally, the ratio of ω^2/v signifies the rotational speed's dominant effect on maximum welding temperature.

Colligan et al. [46] present a conceptual model that relates the main process variables structured around the interrelation of the main categories of physical effects in heat generation and distribution in FSW (see Figure 13). This model distinguishes between the effects of friction and plastic work on heat generation, highlighting that the physical effects include metallurgical, mechanical, heat generation, and heat transfer effects. According to Colligan and Mishra, spindle speed significantly influences power and specific energy, which are directly related to temperature distribution. They state that as spindle speed increases, the resultant in-plane force initially decreases, reaches a minimum, and then increases with further spindle speed increases. This behavior reflects the complex interplay between spindle speed, torque, and heat generation.

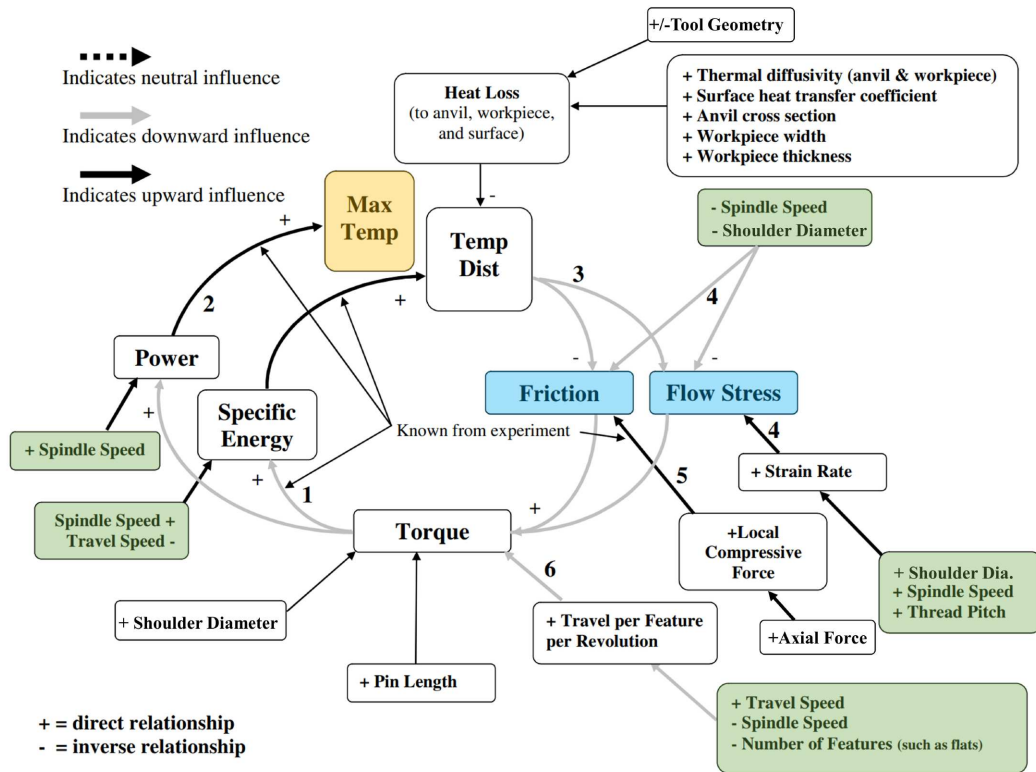


Figure 13: Influence between variables with increasing spindle speed (Colligan et al. [46])

Furthermore, the influence between variables for increasing spindle speed, as shown in Colligan and Mishra's Figure 13, demonstrates that increased spindle speed leads to decreased torque, which exerts a downward influence on power and specific energy. However, increasing spindle speed also exerts an upward influence on power and specific energy, resulting in increased

maximum temperature and temperature distribution. This balance between heat generation and heat loss is critical for understanding the effects of welding parameters on the FSW process.

Peel et al. [39] provide empirical evidence supporting the correlation between predicted and measured thermal histories in FSW. Their study examined dissimilar welds of AA5083 to AA6082 using various welding parameters. They found that the tool's traverse speed influences the heat input inversely compared to the rotational speed. With a fixed rotational speed, increased travel speeds result in reduced temperatures because the material experiences frictional heating for a shorter duration. Conversely, excessively slow travel speeds can cause the material to overheat. Specifically, Figure 14b from their study shows the correlation between the predicted (dotted) and measured (solid) thermal histories for welds produced at 100 mm/min and 300 mm/min traverse speeds with a rotation speed of 840 RPM. The slower travel speed of 100 mm/min results in higher and more widespread temperatures due to prolonged exposure to frictional heating, whereas the faster travel speed of 300 mm/min results in lower and more localized temperatures. Additionally, Figure 14a from their study illustrates the temperature at the location of the backing plate thermocouple as a function of weld pitch. Weld pitch, defined as the ratio of rotational speed to traverse speed, indicates how changes in speed parameters affect the temperature. Peel et al. [39] show that with increasing weld pitch (speed ratio), the temperature decreases, thus confirming that faster traverse speeds and slower rotational speeds lead to lower temperatures.

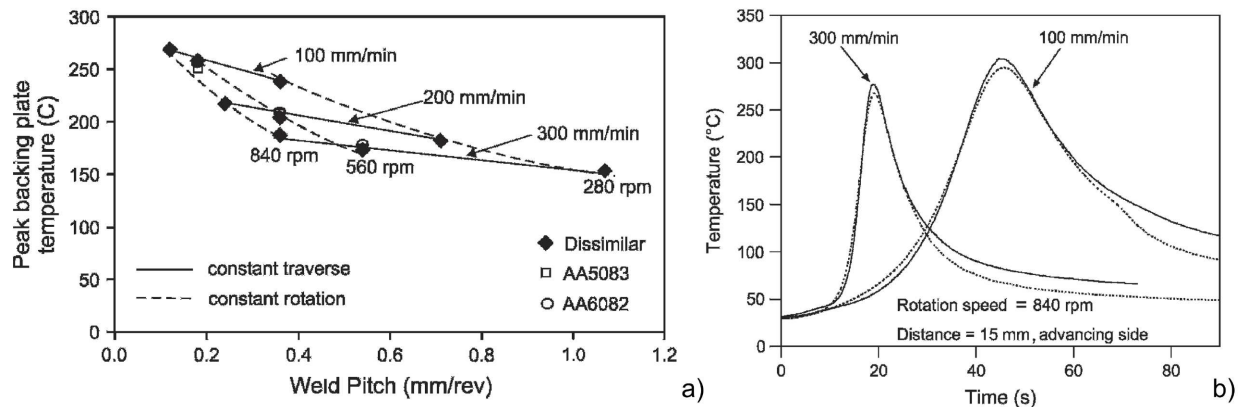


Figure 14: (a) Temperature at the backing plate thermocouple as a function of weld pitch; (b) Difference in temperature on the advancing side of the weld between different weld speeds and constant rotational speed (Peel et al. [39])

The final crucial parameter to address is the axial force. Increasing axial force has a comparable impact on temperature, as does increasing tool rotational speeds (refer to Figure 13). The

heightened pressure enhances frictional heating at the tool-material interface, leading to higher welding temperatures. However, caution is necessary as overheating can adversely affect mechanical properties, and the rotating tool shoulder may penetrate the material surface, causing excess material to be pushed to the weld sides, usually termed flash. Conversely, insufficient axial force prevents the tool from penetrating entirely into the workpiece, causing inadequate contact between the tool shoulder and the component surface. This results in a lack of frictional heating, leading to ineffective stirring and potentially causing internal and/or surface defects. The axial force, for a given set of speed parameters, is crucial in maintaining the quality of the weld. Depending on which parameters are set constant and which are allowed to vary, such as in this dissertation, where the torque is allowed to vary while speed parameters and axial forces are constant, the axial force significantly contributes to the weld quality.

This understanding is crucial for optimizing welding parameters to achieve high-quality welds while minimizing residual stress and distortion. This area of research is particularly important for studying residual stress, as changing multiple parameters, such as welding speeds, affects residual stress, which is further discussed in the section “4.1 Residual Stress Characteristics in Friction Stir Welding: An In-depth Temperature, Texture, and Neutron Diffraction Analysis of Speed Parameters”.

2.2.4 FSW Microstructures

The significant plastic deformation of the weld material and the elevated temperatures during the FSW of aluminum alloys produce a distinctive macroscopic structure, depicted in Figure 15. Moving outward perpendicular from the weld, this structure can be divided into the following unique microstructural zones:

- The stir zone or dynamically recrystallized zone (DXZ) at the weld undergoes both high temperatures and intense plastic deformation due to tool rotation, resulting in grain recrystallization.
- The thermo-mechanically affected zone (TMAZ) experiences high temperatures and some plastic deformation. However, since aluminum can endure significant plastic strain without recrystallizing, this creates a distinct boundary between the recrystallized DXZ and the deformed regions of the TMAZ.

- The heat-affected zone (HAZ) is where the material encounters elevated temperatures that alter its microstructure without inducing mechanical deformation.
- The unaffected base material (BM) is located far from the weld zone, remaining unaffected by the stirring deformation and high welding temperatures.

The grain refinement in the FSW microstructure transpires during mechanical stirring in the DXZ. The tool pin causes intense plastic deformation in the depth of the material, which is pulled up to the shoulder from the retreating side of the weld and deposited on the advancing side, creating a unique marker signifying the advancing side (see Figure 16).

The formation of the DXZ in FSW is heavily influenced by the dynamic recrystallization process, which depends on the temperature and strain rate during welding. Rhodes et al. [47] found that the DXZ develops a fine-grained, recrystallized structure due to the high temperature and extensive localized deformation introduced by FSW. This solid-state process avoids melting, preventing the formation of cast or resolidification microstructures. Instead, a fully recrystallized fine-grain structure is formed within the DXZ. Rhodes et al. [47] highlighted that the peak temperatures during FSW are below the melting point of aluminum, and temperature measurements taken near the DXZ indicate that these peak temperatures are sufficient to cause dynamic recrystallization without melting the material.

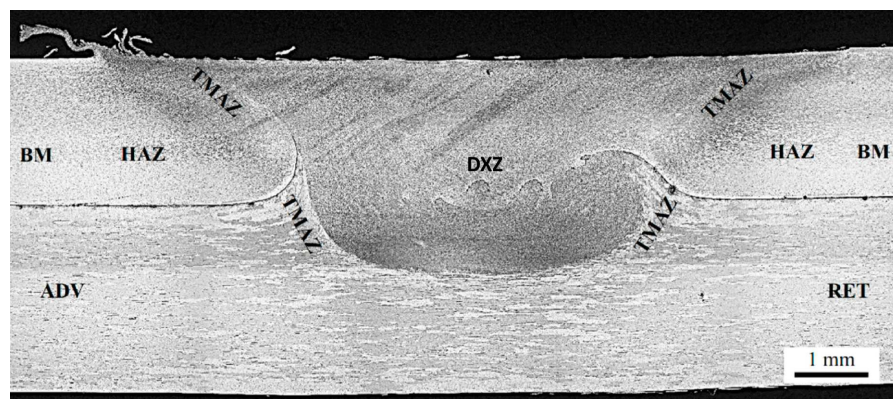


Figure 15: Cross-section of a friction stir weld in a lap configuration (Aldanondo et al. [48])

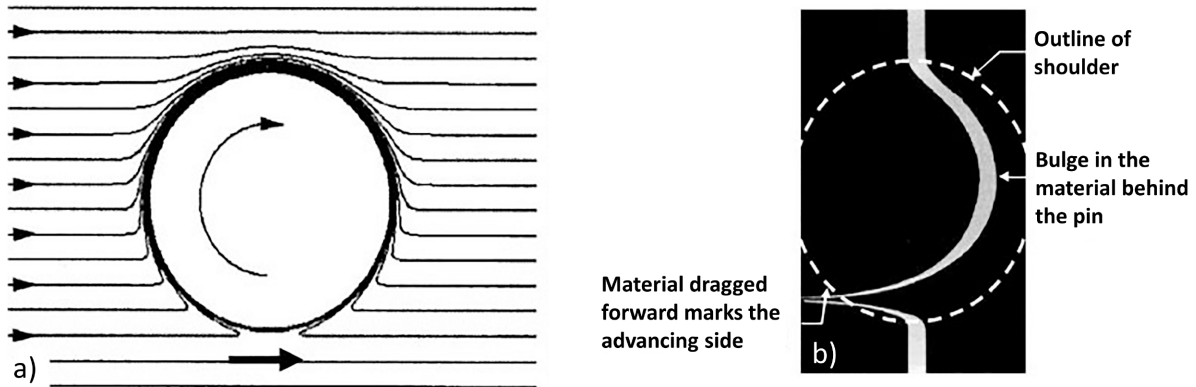


Figure 16: (a) Flow path of surface material around a clockwise rotating pin in friction stir welding; (b) Metallographic marker experiment using transverse copper foil, illustrating internal material flow induced by pin (Seidel et al. [49])

Similarly, Su et al. [50] examined the microstructural evolution in various regions of a friction stir weld and found that the grain structure in the DXZ undergoes significant changes due to the dynamic recrystallization process. This zone experiences the highest temperatures during welding, often exceeding 480°C . This high temperature, combined with severe plastic deformation, leads to the formation of fine, equiaxed grains in the DXZ. Benavides et al. [51] builds on the effects of initial temperatures by studying the effects of low-temperature friction stir welding on aluminum, focusing on how varying the initial temperature impacts the DXZ grain growth. Comparing the grain sizes and microstructures welded at room temperature (30°C) and at a low temperature (-30°C), they found that reducing the initial temperature decreased the DXZ grain size. The grain size at room temperature welding was approximately $10\text{ }\mu\text{m}$, whereas at low temperature, it was reduced to about $0.8\text{ }\mu\text{m}$. This reduction is attributed to the lower temperatures achieved during welding, which limits grain growth (Benavides et al. [51]).

While the initial temperature of the plates significantly affects the DXZ grain size, the effects of changing welding parameters on the DXZ are more complex. Satyavallabh et al. [52] discussed how varying welding parameters, such as rotational and traverse speeds, influence the grain size. They found that the relationship between welding speed and grain size does not seem to directly increase or decrease with welding speed. Their experimental results showed that the average grain size for various welding speeds does not consistently increase or decrease with higher speeds. Instead, grain size reduces to an optimal value and then increases again with further increases in welding speeds, sometimes resulting in significantly larger grain sizes than those observed at slower speeds (Satyavallabh et al. [52]). This may be clarified by the work of Mishra et al. [53],

who presented the work of many researchers studying different magnesium alloys with different welding parameters. They found some general trends, as shown in Figure 17 that a decrease in average grain size is observed with a decrease in tool rotation rate and an increase in tool traverse velocity. When both the rotational speed and traverse speed are increased, as is often the case for industrial welds like in the battery tray, the consideration that now matters is not specifically either the rotational or traverse speed but the ratio of the rotational over the traverse speed (i.e., the speed ratio: ω/v). When the speed ratio decreases, the average grain size also decreases. Although this trend was found for magnesium, Mishra et al. [53] directly states that a decrease in ω/v results in a decrease in average grain size, a fact that is observed for aluminum alloys. This statement implies that keeping this ratio constant could result in consistent average grain sizes for different welding parameters.

This is further supported for aluminum by the a study from Wu et al. [54] that found for constant rotational speed the average grain diameter of the DXZ would decrease as the traversing speed of the tool increased whereas the grain size increased with increasing rotational speed. Two competing mechanisms that may potentially keep the grain size relatively consistent when both rotational and traversing speed are increased at constant speed ratio. Wu et al. [54] determined these relationships using the Zener-Hollomon parameter, which describes the combined effects of temperature and strain rate on the deformation behavior of materials. It is often referred to as the 'temperature-compensated strain rate' because it integrates both the strain rate (rate of deformation) and the temperature into a single parameter (Purkiss et al. [55]). Since the relationship is a negative exponential relationship, an increase in the Zener-Hollomon parameter (which indicates higher temperatures or strain rates) results in a decrease in grain size. When both rotational and traversing speeds are increased by a constant ratio, the temperature increase due to higher rotational speed is partially offset by the reduced heat input per unit length due to faster traversing speed. The strain rate increases with higher rotational speed, promoting finer grains, while the reduced thermal exposure due to faster traversing speed promotes coarser grains. Reduced thermal exposure during faster traversing speeds means that the material spends less time at elevated temperatures. This shorter exposure time limits the extent of dynamic recrystallization and grain refinement processes that typically occur at high temperatures. As a result, the grains do not undergo as much recrystallization and remain coarser. Thus, these two effects counterbalance each other, leading to a net grain size that remains relatively consistent.

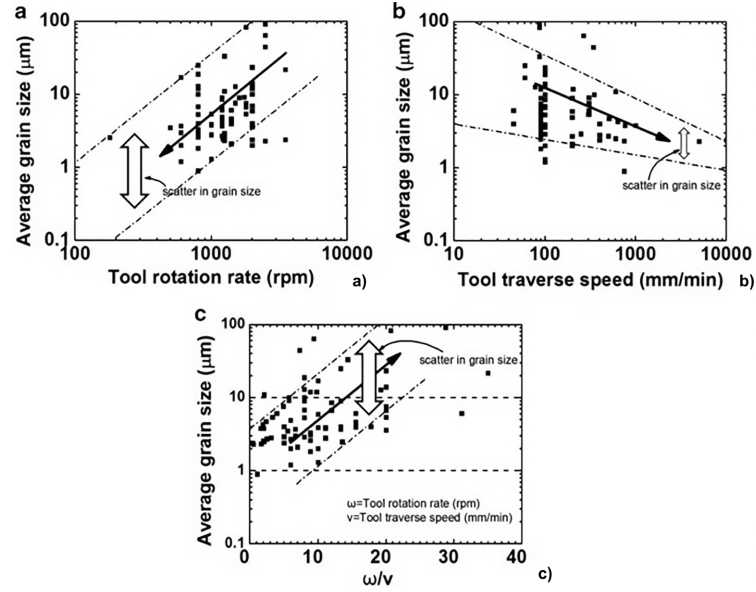


Figure 17: (a) Variation of average grain size as a function of the tool rotation rate, ω ; (b) Tool traverse speed, v ; (c) Ratio, ω/v (Mishra et al. [53])

The dynamics around speed ratios have important implications for the subsequent sections that discuss a constant speed ratio utilized, with changing measured properties and how texture and its effects on mechanical properties, ultimately yield strength, could contribute to affecting residual stress. By maintaining a constant speed ratio, the literature suggests that grain sizes should remain relatively consistent despite changes in rotational and traverse speeds. This is an important consideration when comparing residual stress profiles measured for different welding parameters at a constant speed ratio, as discussed in the results section “4.1.4 Residual Stress on Single Welded Plates” and “4.1.5 Texture Measurements of Single Welded Plates”.

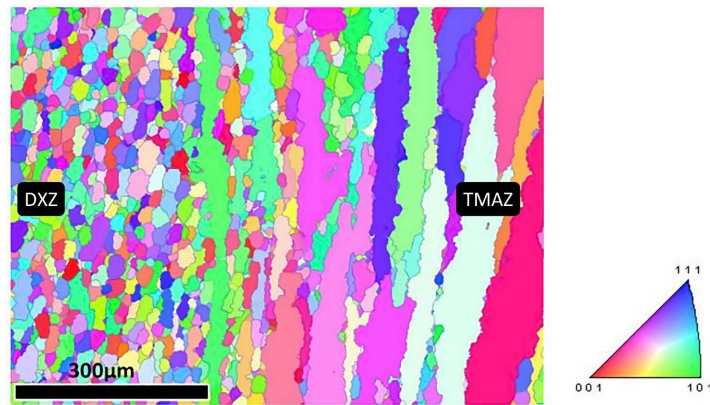


Figure 18: Grain orientation analyzed by Electron backscatter diffraction (EBSD) of DXZ and TMAZ for AA6061-T6 (Fadaeifard et al. [56])

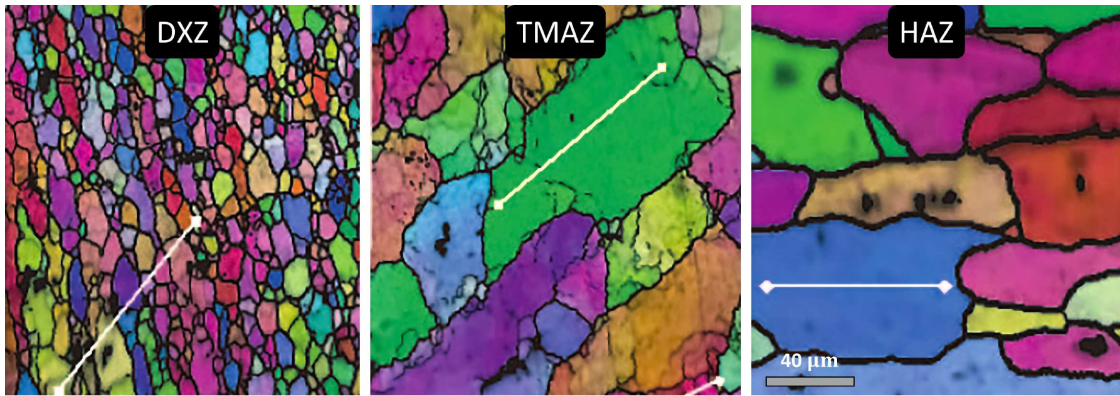


Figure 19: Typical grain sizes of 6061 microstructure in DXZ, TMAZ, and HAZ (Liu et al. [57])

Nevertheless, the direct differences in properties due to the grain refinement between the DXZ and other zones are substantial. Figure 18 displays the direct interface between the DXZ and the TMAZ, while Figure 19 shows the differences between the HAZ and the DXZ/TMAZ. These mechanical properties are discussed in the section “2.2.5 Mechanical Properties of DXZ, TMAZ, and HAZ”. These property differences are an important consideration for yield strength directly across the weld and help to explain the residual stress profiles measured across the weld, particularly the cross-sectional "M" shape observed in Chapters 3 and 4.

2.2.5 Mechanical Properties of DXZ, TMAZ, and HAZ

Following the discussion on the microstructural distinctions between the dynamically recrystallized zone (DXZ), the thermomechanically affected zone (TMAZ), and the heat-affected zone (HAZ) in friction stir welds of aluminum alloys, it is important to explore the mechanical properties of these zones to understand their impact on residual stress profiles in preparation for the discussions in 2.3 Residual Stress in Friction-Stir-Welding.

Lemmen et al. [58] conducted extensive studies on yield strength and residual stress profiles across different aluminum alloys, including AA2024-T3, AA7075-T6, and AA6013-T4. Their research emphasizes the significance of local yield strength variations in defining the residual stress profiles. Lemmen et al. [58] describe that yield strength profiles were obtained through digital image correlation (DIC) to measure local yield strength across the friction stir welds. DIC is a non-contact optical technique that captures images of the material before and during deformation. By analyzing these images, the researchers were able to generate detailed local strain data, which,

when combined with the applied load during tensile testing, provided local stress-strain curves. These curves allowed for precise determination of the local yield strength at intervals of approximately 1 mm. From these measurements, Lemmen et al. [58] obtained yield strength profiles across the different zones of the welds, observing that the DXZ generally exhibits reduced yield strength compared to the base material (BM). The TMAZ, which experiences both thermal and mechanical effects but to a lesser extent than the DXZ, shows intermediate yield strength values (see Figure 20). The HAZ, affected primarily by thermal cycles without substantial plastic deformation, often retains higher yield strength similar to the BM, which is also found in this dissertation in section “4.2.4 Residual Stress of Multi Welded Plates”. The relationship between yield strength and residual stress is particularly evident in the stress profiles. Lemmen et al. [58] found that residual stress profiles in the longitudinal direction exhibit an M-shape, especially in alloys like AA6013-T4, which resembles the stress profiles observed in this dissertation's study of AA6061-T6. They noted that the residual stresses near the edge of the weld, close to the TMAZ and HAZ, are high tensile stresses, which decrease toward the central weld region (see Figure 20a). Lemmen et al. [58] describe how the thermal stress distribution is influenced by the yield strength of the material. During the cooling process, as the material cools and its yield strength increases, the residual stress is limited by this yield strength. The residual stress generally cannot exceed the yield strength, leading to the observed residual stress profiles. This relationship underscores the critical role of yield strength and its dependence on temperature in determining the final residual stress profiles.

These discussions have important implications for this dissertation. By maintaining a constant speed ratio in the welding parameters, relatively consistent grain sizes across different welds can be achieved (as described in section “2.2.4 FSW Microstructures”). Still, much different residual stresses can be measured (see results in Chapter 4). However, with residual stress profiles/maximums and minimums being limited by the yield strength (Lemmen et al. [58]), other mechanisms that affect yield strength must be considered in the case of consistent grain sizes, especially after the effects of temperature on yield strength are considered. This is further discussed in the section “2.3.2 Evolution of Residual Stresses in FSW”. Nevertheless, a potential key mechanism discussed in this dissertation is the influence of texture on changing welding speeds at a consistent speed ratio (therefore, higher temperature, see Equation 1). The mechanism by which

texture affects mechanical properties is discussed in the next section, “2.2.5.1 Texture Considerations”.

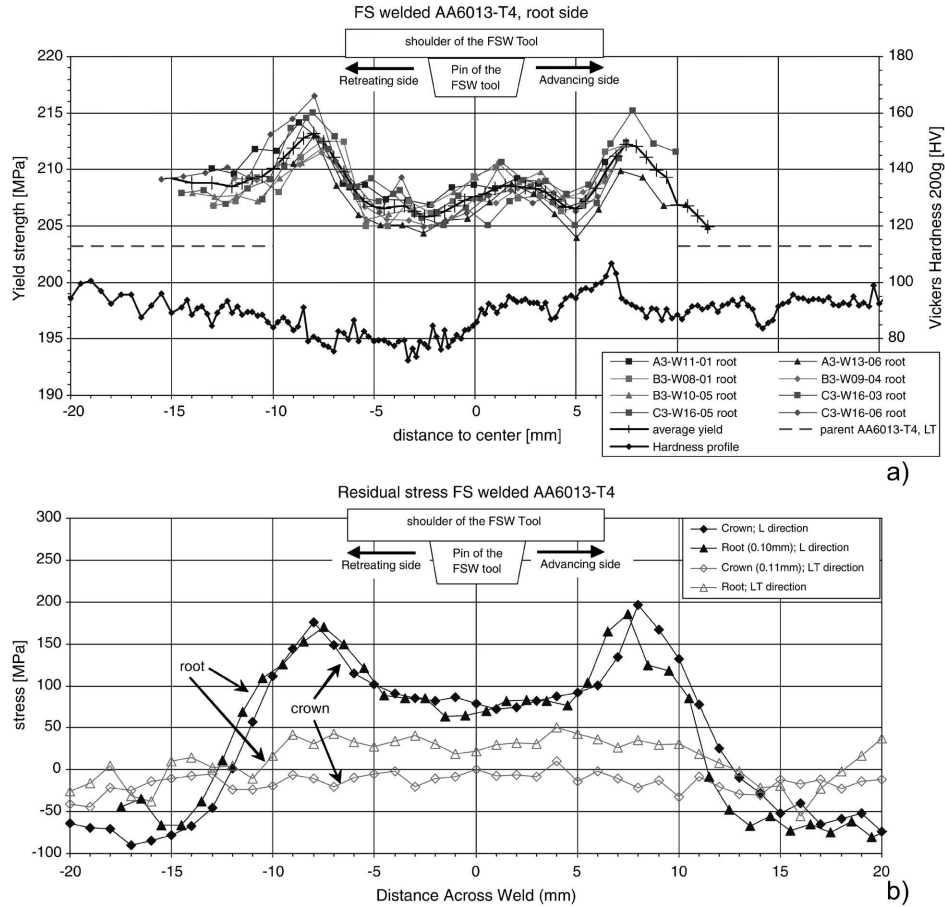


Figure 20: (a) Yield strength profiles of friction stir welded AA6013-T4, compared to hardness profile from an equivalent friction stir weld; (b) Longitudinal, L, and transverse, LT, direction residual stress profile for FS welded AA6013-T4 (Lemmen et al. [58])

2.2.5.1 Texture Considerations

The phenomenon of texture-induced grain convergence is particularly relevant in the context of FSW, especially when examining polycrystal structures like the aluminum alloys discussed in this dissertation. Zhang et al. [59] describe that during severe plastic deformation, extensive grain fragmentation occurs, breaking down the original grain structure into smaller, more fragmented grains, often associated with the formation of a complex profile of crystallographic textures across the weld region. This process occurs before the recrystallization in FSW, where severe plastic deformation in the material leads to grain fragmentation, breaking the initial grains into smaller grains.

Dynamic recrystallization involves the formation and growth of new, less-defective grains within a deforming material. In the context of FSW, the severe plastic deformation and resulting grain fragmentation can stimulate dynamic recrystallization. Essentially, grain fragmentation provides the "raw material" (small grains) that can then recrystallize to form the new grain structure in the weld. However, this process can also lead to grain coarsening as neighboring grains may align to the same ideal orientation, effectively merging into a single grain. This process is referred to as "texture-induced grain convergence" or "texture-induced grain coarsening" (Zhang et al. [59]).

As a material undergoes deformation, the grains within it can change their orientation to match that of their neighbors. When neighboring grains achieve the same orientation, they can merge or "coalesce" into a larger grain. This results in an increase in the average grain size within the material, a phenomenon known as grain coarsening. Importantly, while grains do not necessarily need to physically merge into one larger grain, they align in such a way that they share the same crystallographic orientation, making them behave as if they were a single, larger grain. This effect significantly impacts the mechanical properties of the material, making understanding and controlling texture-induced grain convergence crucial for optimizing these properties.

During FSW, severe plastic deformation leads to changes in the grain structure of the material. As the grains reorient to match their neighbors, they can act as larger grains due to their aligned texture. This grain coarsening can affect the mechanical properties of the welded joint, such as yield strength. Xie et al. [60] found that in friction stir processing (a similar process on non-jointed structures) of an aluminum alloy, texture-induced grain convergence causes neighboring grains to converge under strong texture, which limits the extent of grain refinement. They also found that the texture is negatively correlated to the amount of high-angle grain boundaries or misoriented grains, which is also discussed in section "3.2.3 EBSD".

In the context of FSW, if the texture-induced grain convergence process leads to large regions of similar texture (i.e., many small grains aligning in the same direction), it could result in a situation where the material behaves as if it had larger grains. The strength of a material with small grains can be attributed to the randomness or disorientation of the grains. This effect on strength is known as grain boundary strengthening or the Hall-Petch effect, which states that the yield strength of a material increases as the grain size decreases. In a material with small but randomly oriented grains, the grain boundaries act as barriers to dislocation movement, which is a primary mechanism

of deformation in crystalline materials. The more grain boundaries there are (i.e., the smaller the grains), the more barriers there are to dislocation movement, and thus, the stronger the material becomes Armstrong et al. [61]). However, if the grains are extremely small but all aligned, the grain boundaries may no longer act as effective barriers to dislocation movement. This inefficient barrier is due to the dislocation's ability to move along the common crystallographic direction shared by the aligned grains. As a result, the material may exhibit lower yield strength compared to a material with the same small grains but more randomly oriented.

This leads to an interesting dynamic where, if the speed ratio is kept constant, the grain sizes, as mentioned in section “2.2.4 FSW Microstructures”, remain consistent. By increasing the welding speed, the temperature increases (refer to Equation 1), but the grains are not being refined any smaller. Instead, it is observed that either increasing or decreasing welding speed parameters, even though it affects temperature, does not necessarily refine the grain size further. Hence, it's worth considering the alignment or randomness of the grains, which is influenced by welding parameters, determines many of these mechanical properties, particularly the yield strength and how that may impact the residual stress profiles.

In support of the statement of how texture effects the yield strength, Woo et al. [62] friction stir processing (FSP) a magnesium alloy, considered two cases; Case 1 refers to the FSP plate that was processed using both the stirring pin and tool shoulder, which induced both heating and deformation during FSP. In this case, the severe plastic deformation caused significant texture changes, resulting in a localized softening effect in the DXZ. Case 2 refers to the FSP plate that was processed under the same conditions as Case 1 but using a special tool without the stirring pin. This setup minimized the plastic deformation, isolating the thermal effects. Woo et al. [62] found a sharp decrease in residual stress in Case 1's stir zone and attributed it to the severe plastic deformation from the tool pin, which caused significant changes in the material's texture. This localized texture change led to a reduction in yield strength (softening effect), thereby decreasing the residual stress. This phenomenon was not observed in Case 2 because the tool without the stirring pin did not cause the same level of plastic deformation, resulting in a different residual stress profile without a sharp decrease. However, Woo et al. [62] did not attribute the described outcome specifically to texture-induced grain coarsening, instead to the increase in Schmid factor (Schmid et al. [63]).

The Schmid factor (m , see Equation 2) is a measure used to describe the ease with which a particular slip system in a crystal structure can be activated under applied stress. It is an important parameter in understanding and predicting the onset of plastic deformation in crystalline materials. The Schmid factor is defined as:

$$m = \cos \phi \cos \lambda \quad \text{Equation 2}$$

Where ϕ is the angle between the applied stress direction and the slip direction, and λ is the angle between the applied stress direction and the normal to the slip plane. In Equation 3, the Schmid factor determines the resolved shear stress (τ) on a slip system for a given applied stress (σ):

$$\tau = \sigma m \quad \text{Equation 3}$$

A high Schmid factor means that a lower applied stress is required to activate the slip system, facilitating plastic deformation. Conversely, a low Schmid factor means that higher stress is needed to cause slip. Aluminum, like many face-centered cubic (FCC) metals, has multiple slip systems due to its high symmetry. The primary slip system in FCC metals, including aluminum, is the $\{111\}$ plane in the $\langle 110 \rangle$ direction (see Figure 21).

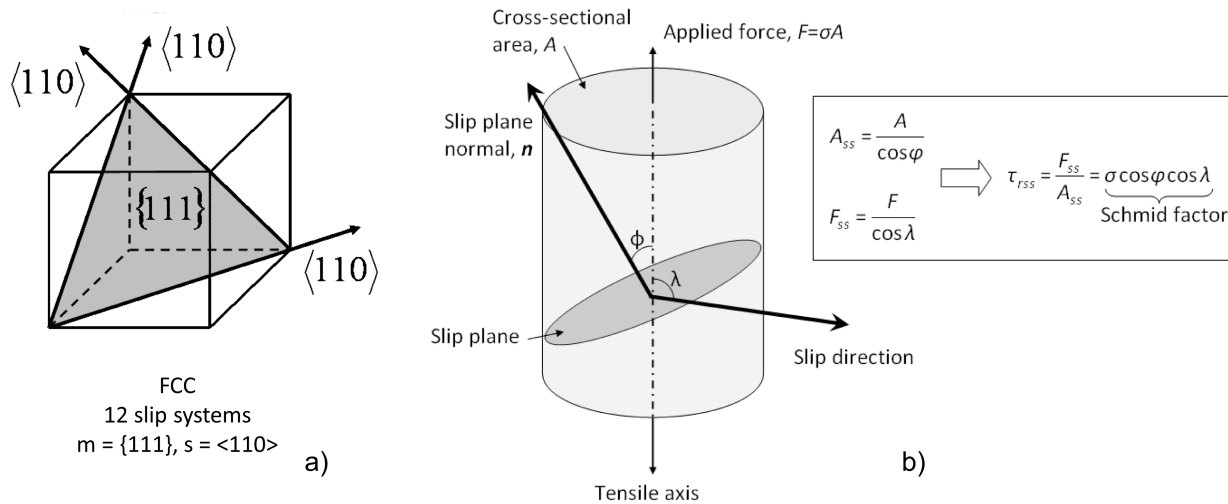


Figure 21: (a) Predominant slip systems in cubic lattices; (b) Derivation of Schmid factor for a single slip system (Schmid et al. [63]; Ennis et al. [64])

The consideration of the Schmid factor is essential in aluminum for the following reasons:

- Predicting Yield Strength: By understanding which slip systems have the highest Schmid factors, one can predict the yield strength of aluminum under different loading conditions.
- Texture and Anisotropy: The distribution of Schmid factors among different grains in polycrystalline aluminum affects the material's texture and anisotropic mechanical properties. For instance, during processes like rolling, certain crystallographic orientations become more prevalent, influencing the overall deformation behavior.
- Deformation Mechanisms: The ease of activation of various slip systems, as indicated by the Schmid factor, helps in understanding the mechanisms of plastic deformation in aluminum, especially under complex loading conditions.

Ultimately, the Schmid factor may be a significant factor in explaining yield strength reduction during texture-induced grain convergence in aluminum. In aluminum, during processes such as rolling, extrusion, or friction stir welding, certain crystallographic orientations become more prevalent, leading to a preferred texture. This means that the grains are oriented in a way that specific slip systems are more aligned with the direction of applied stress. The Schmid factor determines the ease with which these slip systems can be activated. If the texture alignment increases the Schmid factor for the primary slip systems, the material will deform more easily in the direction of applied stress. Conversely, if the texture alignment decreases the Schmid factor, higher stress will be required for deformation.

When grains converge in a texture that increases the Schmid factor for the dominant slip systems, the resolved shear stress on these slip systems increases for a given applied stress. This makes it easier for the material to deform, effectively lowering the yield strength plastically. In other words, the material yields (begins to plastically deform) at a lower applied stress because the slip systems are more favorably oriented. For example, in rolled aluminum sheets, the rolling process can produce a texture where the $\{111\}$ planes and $\langle 110 \rangle$ directions (the primary slip systems for FCC metals; see Figure 21) are aligned with the rolling direction. This alignment increases the Schmid factor for these slip systems, leading to easier activation under applied stress. Similarly, in FSW aluminum, the severe plastic deformation and thermal cycles can create a texture that enhances the Schmid factor for certain slip systems, thus reducing the yield strength due to easier slip activation.

Thus, understanding texture-induced grain coarsening is an important consideration for this dissertation. This alignment and/or randomness affects residual stress indirectly by first

influencing yield strength, which then limits the maximum residual stress. However, as mentioned, this only affects the maximum residual stress; the generation of residual stress is driven by other mechanisms like temperature gradients and the resulting misfits, as discussed in section “2.3 Residual Stress in Friction-Stir-Welding”. Therefore, understanding the effects of texture during FSW for varying welding parameters is important for optimizing the mechanical properties of the weld and minimizing detrimental residual stresses, as discussed in the section “4.1.5 Texture Measurements of Single Welded Plates”.

2.2.5.2 Strain Rate Sensitivity Considerations

Strain rate sensitivity is a significant factor in the context of FSW for polycrystalline structures like 6061 aluminum alloys. Strain rate sensitivity refers to the change in a material's flow stress (the stress required for continuous plastic deformation) with varying strain rates. Materials with high strain rate sensitivity exhibit significant increases in flow stress as the strain rate increases. This characteristic is particularly relevant for 6061 Aluminum T6 at the high temperatures encountered during FSW.

During FSW, the material undergoes severe plastic deformation as the rotating tool plunges into the workpiece and traverses along the joint line. This process generates significant heat due to friction and plastic deformation, raising the temperature of the material around the tool. As the traverse speed and rotational speed increase, competing mechanisms affect the thermal and deformation characteristics. The increased rotational speed generates more heat through friction and large deformations, while the faster traverse speed reduces the exposure time to elevated temperatures. Consequently, the overall temperature increases, but the width or breadth of the temperature distribution decreases.

The decrease in temperature distribution width is important because it limits the area affected by high temperatures, leading to a more localized heating effect. This localized heating influences the dynamic recrystallization process within the dynamically recrystallized zone (DXZ). Dynamic recrystallization, where new grains form and grow during deformation, is significantly affected by the strain rate sensitivity of the material. Higher strain rates can lead to more pronounced grain fragmentation (where grains break into smaller pieces) and distinct reorientation patterns compared to lower strain rates. These changes impact the crystallographic texture or preferred orientation of grains.

At elevated temperatures, aluminum alloys undergo two main softening mechanisms: dynamic recovery and dynamic recrystallization. Dynamic recovery involves the rearrangement of crystal defects, reducing dislocation density and softening the metal. Dynamic recrystallization, on the other hand, begins when the material reaches its limit for storing immobile dislocations as a result of strain hardening and recovery. It involves the formation and growth of new grains, significantly affecting the microstructure and mechanical properties of the material. Dynamic recrystallization is particularly influential in modifying the crystallographic texture, which affects material anisotropy. Aluminum, due to its high stacking-fault energy, typically exhibits dynamic recovery, causing the flow stress to reach a steady state after initial hardening. However, under conditions where dynamic recrystallization occurs, such as during high-temperature deformation, the material's texture and mechanical properties can change significantly (Salvado et al. [65]).

An important aspect to consider in this context is the sticking and sliding mechanism. When the material sticks to the tool, it undergoes severe plastic deformation, resulting in higher strain rates in these regions. Higher strain rates in the sticking region lead to greater resistance to deformation (higher flow stress) and influence the texture by activating different slip systems. Conversely, in regions where the material slides over the tool without significant sticking, the strain rate is lower, leading to less resistance and potentially different texture patterns.

The transition between sticking and sliding is influenced by temperature, with higher temperatures promoting more sliding and less sticking. This introduces competing mechanisms: in sticking regions, the high strain rate at higher speeds increases flow stress and modifies texture. In sliding regions, which become more prominent at higher speeds, the texture may remain similar to that observed at lower speeds due to lower strain rates. At higher strain rates, certain slip systems that typically remain inactive may become more active, leading to distinct texture variations as speeds increase.

In all cases, it is expected that FSW will result in the formation of a strong texture, where many grains are aligned in a specific orientation, affecting the mechanical properties of the weld. Texture-induced grain convergence, where grains with similar orientations coalesce or align, effectively behaving as larger grains, can be significantly affected by strain rate sensitivity. The alignment of grains in a specific orientation can impact the yield strength and other mechanical properties of the weld. Higher strain rates can lead to textures that either enhance or reduce yield

strength, depending on the specific orientation of the grains and the direction of the external loading.

Understanding the strain rate during FSW is important for recognizing the effects of welding parameters such as tool rotation speed and traverse speed. Strain rate sensitivity plays a key role in the creation of texture during FSW of 6061 aluminum T6. The high-temperature deformation inherent in FSW leads to varying strain rates, which significantly affect dynamic recrystallization, grain fragmentation, and texture development.

2.2.5.3 Precipitation Considerations

In the context of FSW of aluminum alloy 6061, both texture-induced grain coarsening and precipitation strengthening can play a role in determining the yield strength of the weld. However, these two mechanisms operate in different ways and are influenced by different factors. According to the study by Hakem et al. [66], investigations have shown that the heat-affected zone (HAZ) experiences dissolution, over-aging, and coalescence of precipitates, leading to a decrease in hardness and strength. In addition to the HAZ, the microstructural zones within the weld, such as the dynamically recrystallized zone (DXZ) and the thermomechanically affected zone (TMAZ), also show variations in precipitation behavior. The study indicates that the DXZ often contains fewer precipitates due to the high temperatures during welding, which can dissolve existing precipitates.

Initial studies by Liu et al. [67] revealed that in the base metal of FS-welded 6061-T6 aluminum alloy, precipitates were homogeneously distributed. However, after FSW, the DXZ exhibited far fewer precipitates, and those that were present were larger compared to those in the base metal. Liu et al. [67] also noted that the process involved a continuous increase in misorientation within the grains, which is different from classical recrystallization involving nucleation and growth of new grains. This aspect of texture is important in understanding the material's mechanical properties post-welding.

Sato et al. [68] used transmission electron microscopy to examine the microstructural evolution of Al 6063-T5 during FSW. They found that the DXZ lacked any precipitates, indicating that all the strengthening precipitates dissolved in the aluminum matrix during FSW. This dissolution contributes to the understanding that the DXZ, characterized by fine-grained microstructures, may

rely less on precipitation strengthening and more on grain boundary strengthening. Therefore, changes in the yield stress from one weld parameter to the next must be due to these grain boundaries. However, if the grain sizes are maintained at a constant speed ratio, then other mechanisms must be considered. This suggests that other factors, such as texture-induced grain coarsening, might have a more significant impact on the mechanical properties of the weld.

2.2.6 Section Summary

The exploration of FSW in the above literature section highlights several important aspects influencing the mechanical properties and residual stress profiles of aluminum alloys. Fusion welding presents numerous challenges for aluminum, including issues with melting points, oxide formation, and hydrogen solubility, making solid-state processes like FSW a preferable alternative. FSW avoids the complications associated with melting, significantly reduces the risk of porosity, and provides robust joints through mechanical stirring and plastic deformation. A review of the operating principles of FSW, including the plunging, traversing, and tool withdrawal stages, underscores the importance of understanding heat generation and its non-uniform distribution around the tool (i.e., advancing vs. retreating sides). The discussion extends to the advantages of using Smoothed Particle Hydrodynamics (SPH) simulations over traditional Finite Element Method (FEM) simulations, particularly in accurately predicting temperature distributions, understanding material flow, and handling large deformations inherent in the FSW process. Key welding parameters such as rotational speed, traverse speed, and axial force were reviewed, illustrating their complex interactions and impacts on weld quality. Increased welding speeds while maintaining weld quality is required to meet industrial demands. Keeping certain variables constant, such as the speed ratio and axial force, is important to understand the effects of varying speed parameters on many properties measured throughout this dissertation. The review of microstructural changes in FSW highlighted the formation of distinct zones within the weld, with dynamic recrystallization being important for grain refinement. However, the phenomenon of texture-induced grain convergence, where grains align and behave as larger grains, can affect yield strength and, consequently, the residual stress profile. This review identifies gaps in understanding how texture-induced grain convergence influences yield strength and residual stress, particularly in the context of FSW.

Few studies have addressed the effects of texture on yield strength and its impact on residual stress in FSW aluminum alloys; specifically, the consideration of texture coarsening in FSW is only available in a few literature sources (Mironov et al. [69]; Xie et al. [70]). Therefore, this dissertation aims to help fill this gap by analyzing the microstructure, texture, temperature, and stress evolution using applicable industrial speed parameters on lab-generated samples (refer to Chapter 4).

2.3 Residual Stress in Friction-Stir-Welding

This section reviews the fundamentals of residual stress generation from a general welding standpoint in section “2.3.1 Residual Stress” before introducing the residual stress explicitly generated during FSW. From a broad welding perspective, a better correlation is made between the residual stress distributions and the welding parameters of FSW. Lastly, the relationship between residual stress and distortion is discussed.

2.3.1 Residual Stress

Residual stresses remain in a stationary part that is in equilibrium with its surroundings after the initial cause of the stress is removed (Withers et al. [71]). Most typically, residual stresses are caused by sharp thermal gradients, an elastic mismatch between different phases within the material, or non-uniform plastic deformation (see Figure 23). These factors cause residual stress because, in multiphase materials, there can be differences in thermal expansivity, yield stress, or stiffness (Withers et al. [72]). It is important to consider that stress is caused by elastic strain; this is independent of plastic deformation. However, non-uniform plastic deformation can put applied stress on elastic material, consequently creating residual stress by elastic strain. If the material is entirely and uniformly plastically deformed, there can be no measurement of residual stress. In practice, plastically deformed materials always contain elastic components that are measurable as elastic strain and, therefore, residual stress. For example, the generated heat during welding expands the local material. This expansion is either taken up by the molten and/or softened material or by the placement of welded parts. Residual stresses then arise due to the differential thermal expansion and contraction of some areas compared to others, leaving some expanded material, constantly imparting a force on other elastic material, producing compressive residual stress. In other cases, the material is contracted, pulling on adjacent materials, resulting in tensile residual

stress. Consequently, residual stresses are self-equilibrating; if some component material is in tension, then elsewhere in the component, there must be material in compression so as to have a zero net internal force. In Figure 22, self-equilibrating residual stress is depicted for a polycrystalline specimen, such as an aluminum alloy. The various colors represent different grain orientations within the magnified portion of the specimen. Internal residual stresses correspond to the characteristic length at which they self-equilibrate. Macro-stress or Type 1 stress (σ_I) is the average stress across a large area, encompassing multiple grains. Second-order or Type 2 stresses (σ_{II}) occur within individual grains or groups of grains in a polycrystalline material. These stresses arise from differences in the behavior of grains that share similar crystallographic orientations but experience different local environments, such as variations in grain boundary constraints or neighboring grain interactions. Third-order or Type 3 stresses (σ_{III}), on the other hand, are even more localized and occur at the scale of the microstructure within a single grain, such as dislocation structures, precipitates, or other micro-defects. These stresses represent the fine-scale variations from the average stress within a grain family, reflecting the influence of very localized microstructural features (Daniel et al. [73]). In Figure 22, compression regions are followed by a shift to tension and vice versa, as shown in the stress vs length plot across the specimen. Additionally, Figure 22 illustrates a crucial aspect: the impact of grain orientation and size on Type 2 and 3 stresses. This effect is particularly significant when calculating residual stress, as it affects the ‘stress-free’ references, a key component in these calculations. Further discussion on ‘Stress-Free Material’ can be found in section “2.4.3.4 Stress-Free Material”.

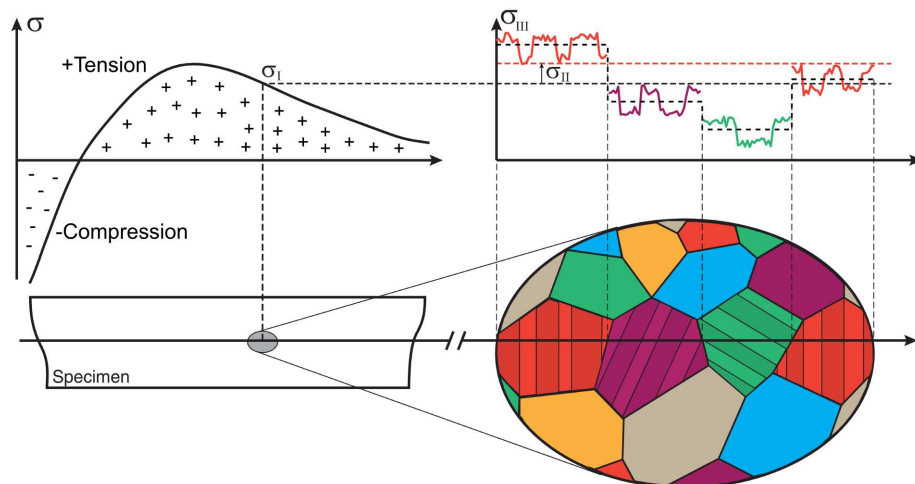


Figure 22: Schematic view of internal stresses in a polycrystalline specimen (Daniel et al. [73])

Figure 23b demonstrates the stress transitions using a welding example, where the weldment separates in tension, and the heat-affected material compresses, as illustrated by the directional arrows. The thermally and mechanically induced residual stresses in welding are Type 1 macrostresses, which vary continuously over long distances (see Figure 23b). Type 1 macroscopic residual stress is vital for assessing the lifespan of metallic components because they balance external forces or self-equilibrate throughout the specimen (Withers et al. [71]). These stresses are significant because they affect the entire component, influencing its load-bearing capacity and creating or amplifying stress concentrations. Managing Type 1 stress is crucial to ensure that the component can withstand operational loads without premature failure. They are also essential for maintaining the structural integrity of the component, as any imbalance can lead to large-scale deformation or failure. Consequently, industrial studies often prioritize Type 1 stresses, as they can significantly reduce a component's performance. Moreover, in plastically deformable materials, uncontrolled residual stress hastens plastic deformation, leading to distortion and, eventually, cracking. Now, it is ideal to consider how residual stresses develop and differ between fusion welding and friction stir welding. Fusion welding involves melting the base material to create a weld pool. As the material cools and solidifies, it forms a cast structure. The primary mechanism for residual stress generation in fusion welding is thermal contraction during cooling. As the molten material cools, it contracts, and the surrounding cooler material constrains this contraction, leading to the development of tensile residual stresses in the weld zone and compressive stresses in the adjacent regions. The temperature gradient in fusion welding is steep, with the highest temperatures at the center of the weld pool and decreasing outward. Consequently, the residual stress profile typically peaks at the center of the weld, where the material was molten, due to the maximum thermal contraction occurring in this region. The residual stress is largely influenced by the thermal cycle, with less impact from the texture of the material since the solidification process results in a relatively random grain orientation similar to casting (see Figure 23c).

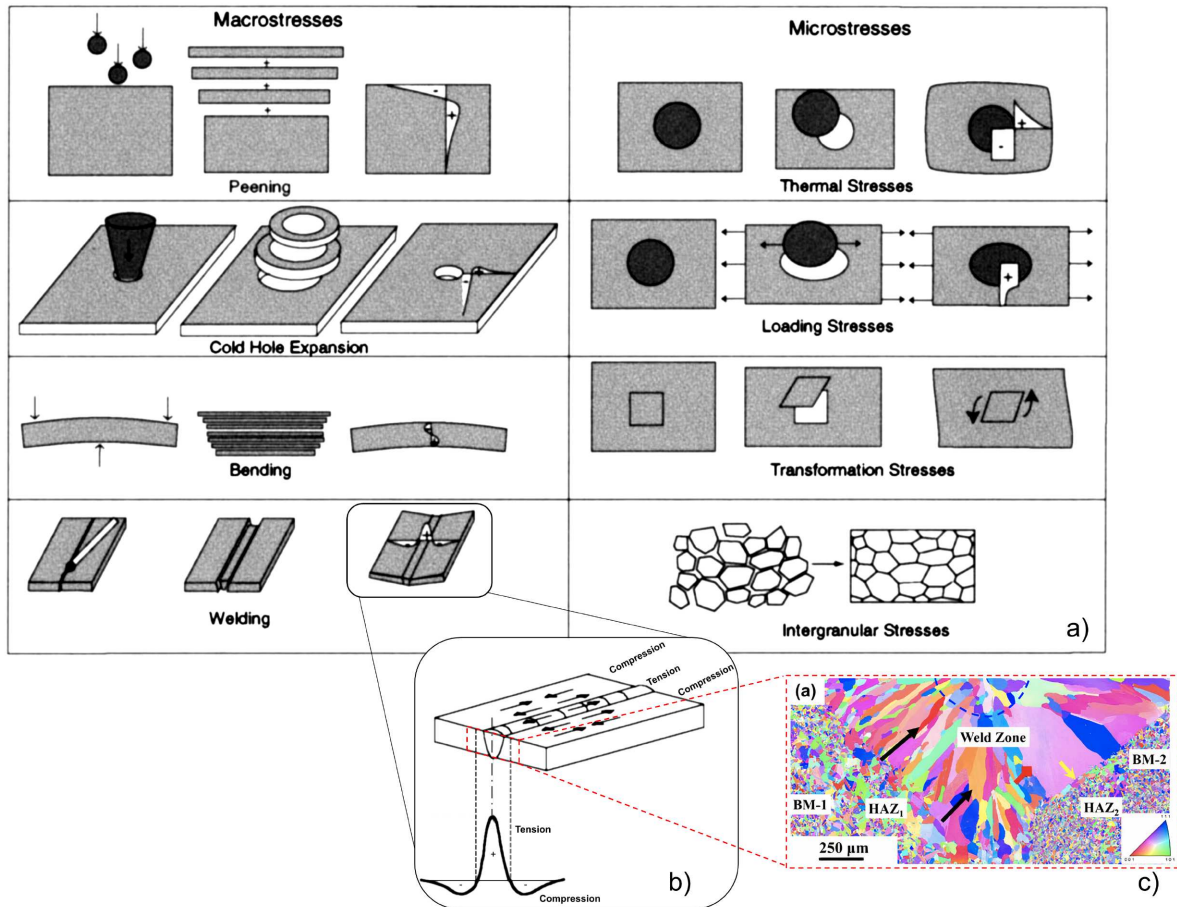


Figure 23: (a) Origins of macro and micro residual stresses; (b) Fusion welding residual stress profile; (c) "Cast-like" structure of fusion welds (Withers et al. [71]; Gordo et al. [74]; Sokkalingam et al. [75])

In contrast, FSW is a solid-state welding process that does not melt the material. Instead, a rotating tool generates heat through friction and mechanically mixes the plasticized material. The material undergoes severe plastic deformation and dynamic recrystallization, creating a fine-grained microstructure. The heat generated by friction and plastic deformation causes thermal expansion of the material around the tool. As the tool moves, the material behind it cools and contracts. Unlike fusion welding, FSW involves significant mechanical deformation, which contributes to the residual stress distribution. The residual stress profile in FSW is often characterized by an "M-shape," with lower residual stress in the center of the weld and higher stresses on the advancing and retreating sides (further discussed in section "2.3.2 Evolution of Residual Stresses in FSW"). This is due to the solid-state nature of the process, where plastic deformation and texture play significant roles. The central region undergoes more uniform deformation and thermal cycling, reducing residual stress. Texture has a considerable impact on FSW, as the alignment of grains

due to mechanical stirring can affect how residual stresses develop and distribute across the weld. The fundamental differences between fusion welding and FSW—melting versus solid-state processing and minimal versus significant mechanical deformation—lead to distinct residual stress profiles. Fusion welding results in maximum residual stress at the weld center due to thermal contraction, while FSW produces an "M-shaped" stress profile with lower stress at the center and higher stresses on the sides, influenced by plastic deformation and texture.

In either case, more and more attention is given to residual stress generation during manufacturing in component design, as if managed correctly, lighter and more robust components can be achieved. All materials have a usable stress limit; when that stress limit is exceeded, the component will distort and eventually crack or break. If the manufacturing process generates tensile residual stress in the component, that residual stress reduces the usable stress limit of the component. In many cases, more material must be added to the component to strengthen it due to the usable stress lost from tensile residual stress generation during manufacturing. If the residual stress is eliminated, the component no longer requires the extra material, and it can be eliminated, leading to a lighter component.

However, residual stress is not strictly a disadvantage; depending on its condition, residual stresses can be advantageous. For example, cracking caused by fatigue will self-arrest in compressive residual stress fields but propagate in tensile residual stress fields (Woodtli et al. [76]). The shot peening process capitalizes on this fact by producing near-surface compressive residual stress in manufactured parts such as turbine blades, crankshafts, and connecting rods to prevent the initiation and propagation of fatigue cracks. As a result, residual stress can be either harmful or advantageous based on its nature and the operational conditions. Accordingly, residual stress can be considered a design element in mechanical engineering and is incredibly important to consider in welded structures. Thus, it is helpful to understand their development in friction stir welds, as discussed in the next section, “2.3.2 Evolution of Residual Stresses in FSW”.

2.3.2 Evolution of Residual Stresses in FSW

FSW joins materials via heat generated by a rotating tool that is plunged into the workpiece and then advanced along the weld joint, thereby mechanically mixing and forging plasticized material together (El-Sayed et al. [38]). However, the intense thermal gradient created by the rotating tool

generates residual stresses due to the uneven distribution of thermal input and plastic deformation across the weld. A study by Richards et al. [7] describes how residual stresses in FSW arise from plastic misfit strains. Plastic misfit strains occur when different parts of a material undergo varying amounts of plastic deformation, leading to mismatches in their shapes or sizes. In the context of FSW, as the tool moves through the material, it heats and plastically deforms the metal around it. However, not all parts of the material deform uniformly—some areas may deform more than others due to differences in temperature, pressure, and material properties. These differences create misfits or mismatches between the deformed regions and the surrounding material. When the material cools and tries to return to its original shape, these misfits cause residual stresses because the deformed regions are constrained by the less deformed or undeformed areas. In simple terms, plastic misfit strains are the strains that result from uneven plastic deformation, leading to residual stresses as the material tries to adjust to these mismatches. This process is simulated by Richards et al. [7] as follows: the FSW tool approaches the material heating it and expanding the material. However, the material behind and on either side of the hot material is cold; thus, the expanding hot material is constrained, generating compressive stress in the material (see Figure 24 - line 2). This compressive stress increases as the tool approaches and eventually exceeds the local compressive yield strength, leading to compressive plastic deformation and a reduction in the measured compressive stress (see Figure 24 - line 3). After the tool continues to advance, the material behind the tool will cool down as the heat source (rotating tool) moves further away. The heated material now cools and contracts; however, the material behind and on either side of the material is much colder (see Figure 25 - contracting case). Because the neighboring cold material restricts this contraction, tensile residual stress will start to generate (see Figure 24 - line 4).

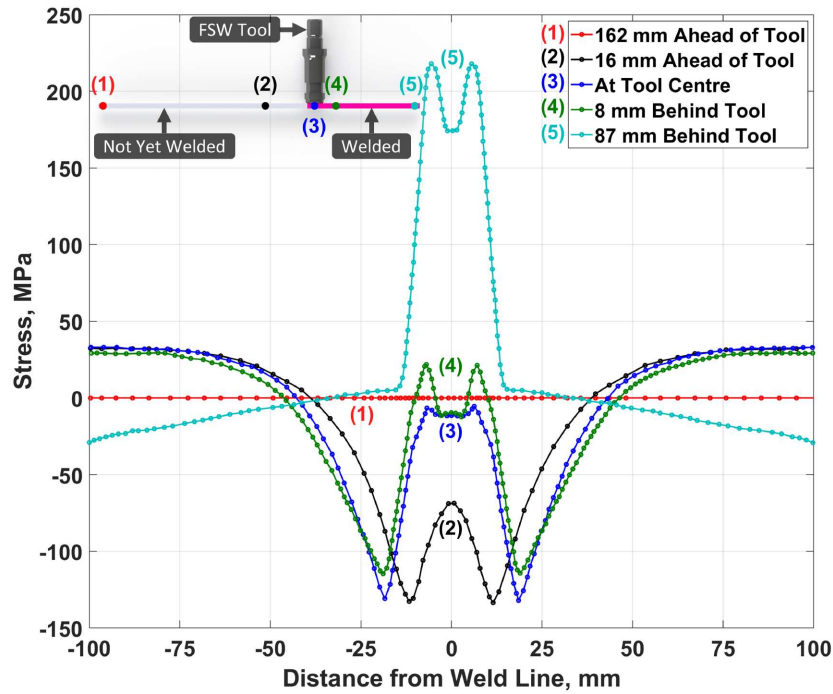


Figure 24: The development of the longitudinal residual stresses' profiles at different distances from the tool, adapted from the work of Richards et al. [7]

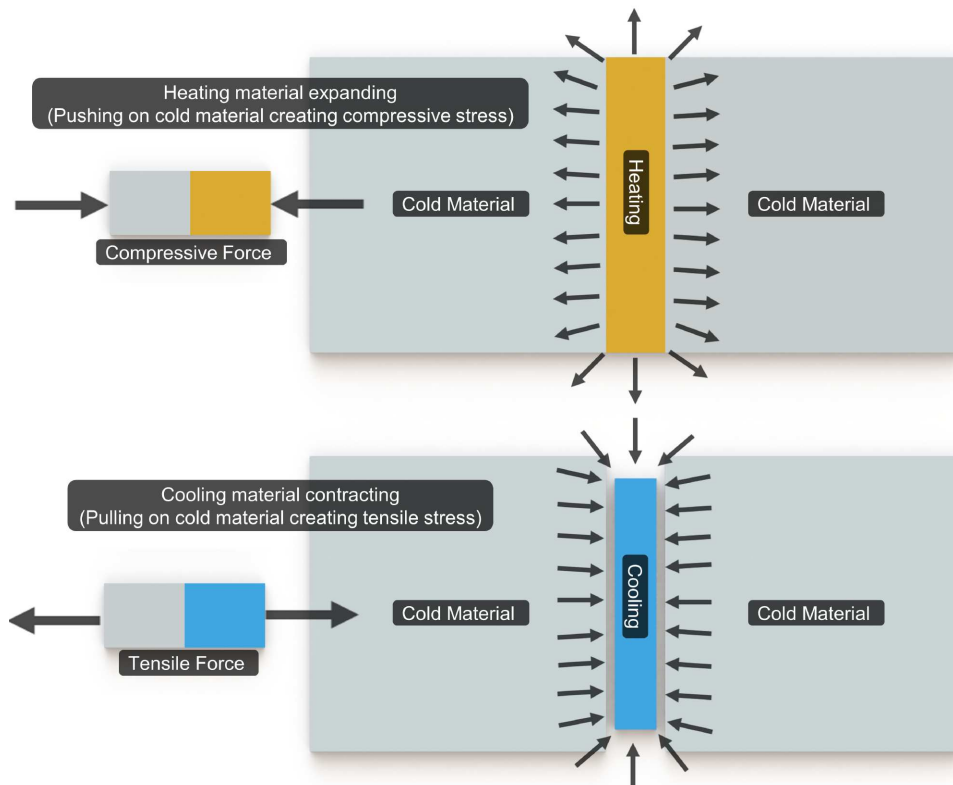


Figure 25: Schematic expansion and contraction of welded material

For high-strength aluminum alloys, the tensile yield stress decreases rapidly with increasing temperature. Consequently, as the material directly behind the tool is at high temperatures, the local tensile yield stress is low. This low yield stress limits the generation of residual stress, as the magnitude of residual stress is determined by the material's elastic strain limit (i.e., before yielding) (see Figure 24 - line 4). The yield stress will continue to increase as the tool traverses further away from the material, reducing the temperature, therefore allowing the tensile residual stress to continue and generate (through a further contraction) the material's elasticity until the yield stress reaches its maximum at room temperature (Figure 24 - line 5). However, many important events occur between line 4 and line 5 in Figure 26, which help explain the "M" shaped final profile. Price et al. [77] effectively explain the temperature-dependent yield strength through a schematic (see Figure 26).

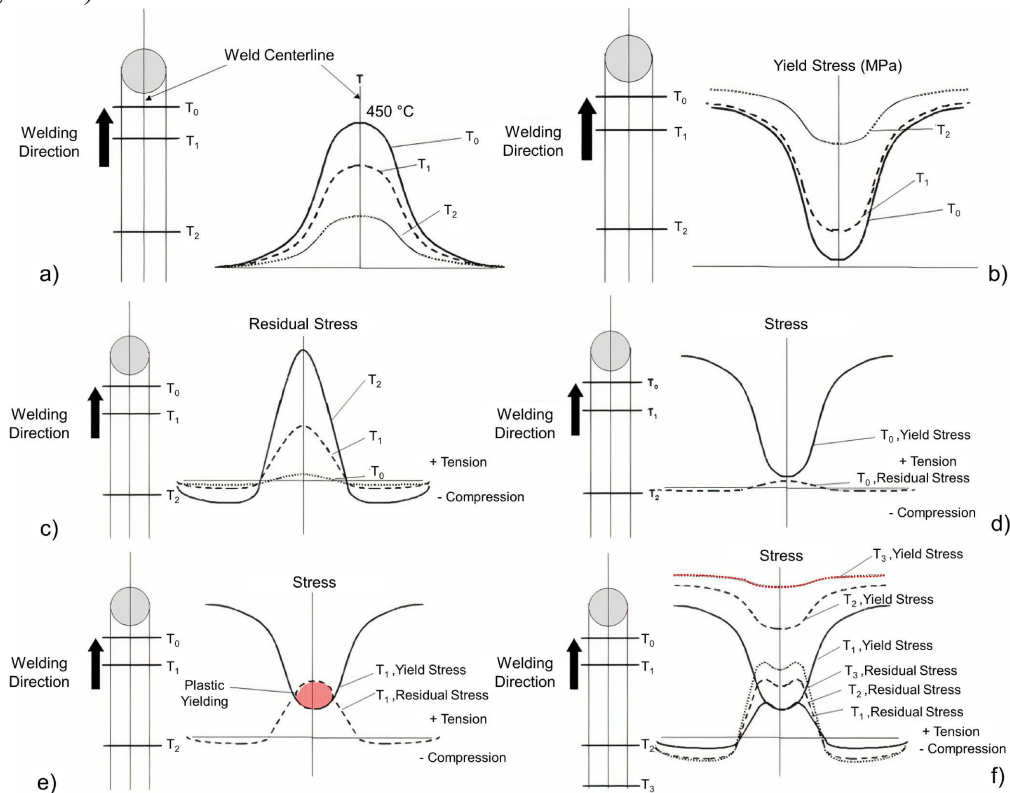


Figure 26: Schematic representation of (a) Temperature variation; (b) Yield stress variation; (c) Residual stress variation; (d) Stress interaction at time T_0 ; (e) Stress interaction at time T_1 ; (f) Resultant residual stress field at different locations behind FSW (Price et al. [77])

This schematic describes that the yield strength of a material is dependent on temperature, which significantly impacts the formation of residual stresses during the welding process. As the FSW tool moves along the weld line, it creates a marked variation in temperature across the weld, which

becomes shallower over time as the tool's distance increases. This variation is schematically represented in Figure 26a by the curves T_0 , T_1 , and T_2 .

Price et al. [77] note that this temperature variation leads to a corresponding variation in the yield stress across the weld (Figure 26b). As the weld cools, a tensile residual stress peak develops across the weld, increasing in magnitude. At the initial temperature T_0 , the tensile residual stress in the weld zone is too low to exceed the yield stress profile (Figure 26d). However, as the temperature drops to T_1 , the tensile stress peak surpasses the yield stress curve. This overlap causes stress relaxation through plastic straining of the hot material (Figure 26e). This plastic yielding imprints the shape of the yield stress curve onto the residual stress curve, producing a small trough. The final shape of the residual stress curve is determined by the position where the curves overlap the most (Figure 26f) (Price et al. [77]). However, there is some contention with the final T_3 yield stress line, depicted by the red dotted line in Figure 26f, returning to near pre-weld yield strength. As mentioned in section “2.2.5 Mechanical Properties of DXZ, TMAZ, and HAZ”, referring to Figure 20, the final yield stress across the weld may not return to near constant yield strength for 6xxx series aluminum but retain the yield strength variations across the weld. Still, if the residual stress matches the yield strength of the material, it signifies that the material is at its elastic limit. Any additional stress applied to the material would instigate plastic deformation. However, it's crucial to note that the distribution of residual stress in a friction stir welded structure is not uniform. Moreover, some researchers (Li et al. [78]; Liu et al. [79]) have found that the ratio between the maximum tensile residual stress and the base material yield strength is approximately 60 to 70% for welded joints under normal friction stir welding conditions. Even in the event that the residual stress does match the yield strength, the structure could still potentially handle an operational load without plastically deforming. This is because the operational load is typically distributed over a large area and across multiple directions, and not all parts or directions of the structure are subjected to the maximum residual stress. In fact, considering other directions is an important aspect. The transverse and normal directions usually exhibit much lower residual stress. This non-uniformity and the multi-directional nature of stress distribution can contribute to the structure's ability to withstand operational loads without deforming. However, areas with high residual stress would be more susceptible to plastic deformation and failure, especially under high operational loads. For instance, the peak value of the residual stress is often located in the thermomechanically affected zone. This zone is prone to fracture due to its relatively poor

mechanical properties. This is an important fact that promoted the multi-directional analysis of residual stress in all three stress directions (longitudinal, transverse, and normal) for this dissertation.

2.3.3 Distortion in FSW

As discussed in previous sections, residual stress can interact with operational stress. This interaction can reduce the total stress that the structure can withstand during operation. If the combined stress exceeds the yield strength of the material, it may cause deformation. This deformation could ultimately lead to the failure of the structure. However, when residual stress alone reaches this threshold, particularly in the context of welding, it is released through plastic or elastic deformation in the affected area, leading to what is termed distortion. In the weld zone, both tensile and compressive residual stress, resulting from nonuniform heating and subsequent cooling, can cause various types of distortion, as illustrated in Figure 27 (Masubuchi et al. [80]).

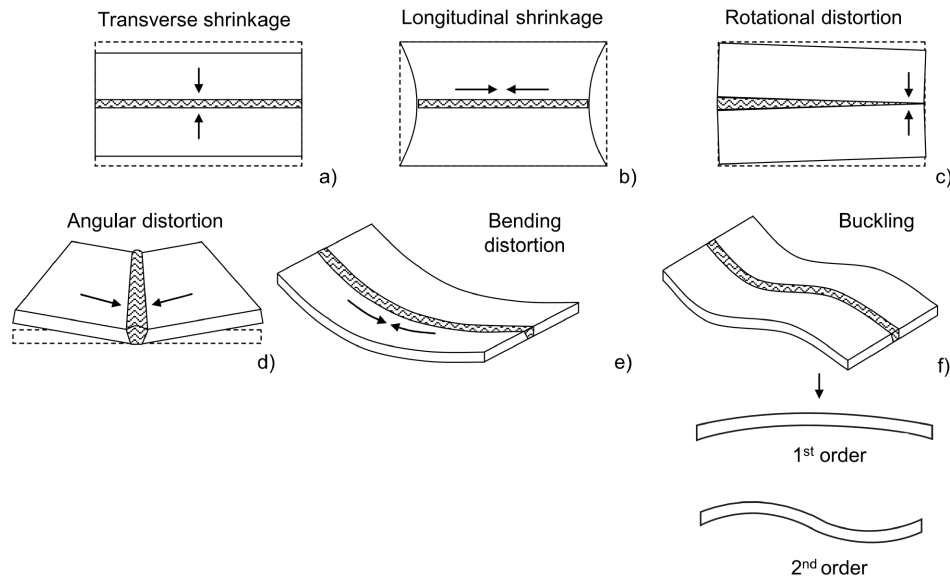


Figure 27: Common modes of distortion that occur during welding (Masubuchi et al. [80]; Van der Aa et al. [81]; Williams et al. [82])

These distortions include angular distortions, which are primarily due to tensile residual stress, and bending distortions, which are also primarily due to tensile residual stress. There's also buckling distortion, which is primarily due to compressive residual stress. Additionally, the distortions encompass transverse and longitudinal shrinkage as well as rotational distortion (Masubuchi et al. [80]). As Williams et al. [82] remark, the applied weld load refers to the load generated by the residual stresses induced during the welding process. These residual stresses, particularly the

compressive residual stresses in the outer regions of the welded plates, create a force or load on the structure. This load can cause various forms of distortion, including buckling if it exceeds the critical buckling load of the plates. The magnitude of this critical load is influenced by the stiffness and geometry of the plates, with thinner and narrower plates having a lower threshold for buckling. The specific buckling mode determines the final distortion's level and shape, which can often resemble bending distortion, making it easy to confuse the two. However, the origins and management strategies for buckling and bending distortions differ. Buckling direction can be highly unpredictable, influenced by variables such as the orientation of pre-existing residual stresses or the clamping configuration used during the welding process (Williams et al. [82]). Nevertheless, for friction stir welds, the resulting distortion is generally accepted to be mostly a combination of angular and a mixture of buckling or bending (El-Moayed et al. [83]; Wen et al. [84]; Altenkirch et al. [85]) resulting in the typical distortion shape seen in Figure 28.

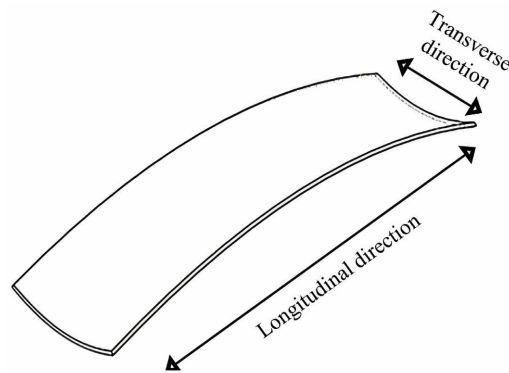


Figure 28: Typical distortion shape of friction stir welds (El-Moayed et al. [83])

According to the research conducted by Masubuchi et al. [80], the extent of distortion is influenced by both the dimensions of the plate and the weld. Masubuchi et al. [80] noted that materials with less thickness tend to display less significant thermal disparities and subsequent plastic misfit, which in turn minimizes deformation. On the other hand, materials of greater thickness have the capacity to resist the residual stress that is responsible for causing distortion. Yu et al. [86], studying welding-induced buckling due to non-uniform stress fields during FSW, noted that the buckling load increases for the structure with a larger thickness. Van der Aa et al. [81] support this statement, stating thicker welds are generally less prone to buckling and tend to deform through bending and angular distortions. In contrast, thinner welds are more susceptible to buckling. Hence, the distortion is significantly affected by material support. Another notable aspect of

material support is the degree of clamping during welding and subsequent cooling, as shown in Figure 29.

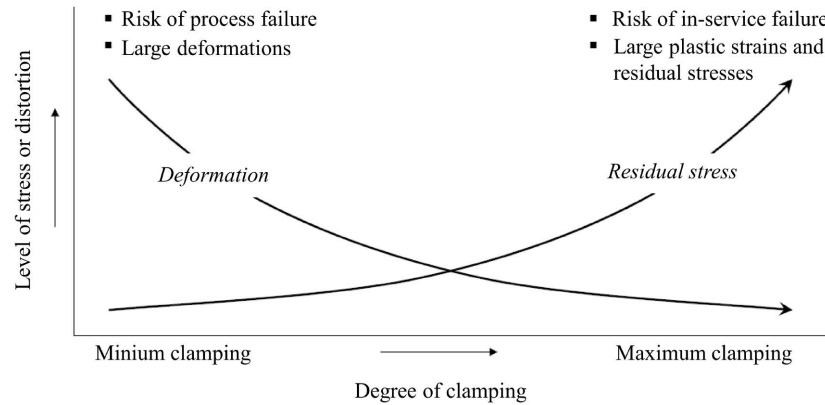


Figure 29: Relationship between the degree of clamping and the level of distortion as well as the effect on residual stresses (Van der Aa et al. [81]; Porzner et al. [87])

Welding with minimal clamping can cause significant deformation but low residual stress, while rigid clamping results in high residual stress but less deformation (Porzner et al. [87]). However, specific clamping configurations are beyond the scope of this dissertation. This dissertation investigates the distortion of full industrial components (e.g., battery trays) and welded plates using practical stress engineering techniques. The goal of stress engineering is to control and reduce residual stress, which is closely related to distortion in weldments. Successful application of these techniques should mitigate component distortion. The dissertation considers several methods of managing residual stress and distortion, referred to as "straightening" operations. These include the burnishing and coining operations discussed along with other methods for controlling distortion in the section "2.3.4 Minimizing Residual Stress and Controlling Distortion in FSW", with the results presented in Chapter 3. Chapter 4 delves into more fundamental considerations, such as how varying welding speeds affect distortion and how multiple welds and their proximity may affect residual stress and, consequently, distortion.

2.3.4 Minimizing Residual Stress and Controlling Distortion in FSW

The formation of residual stress is an inherent aspect of material processing, but with appropriate control of process input parameters, their impact can be minimized up to a certain limit. Beyond this limit, secondary thermal or mechanical loads are required to reduce residual stress further. This section will review a few essential techniques that researchers and industry utilize to mitigate the effects of residual stress.

2.3.4.1 Thermal Tensioning

In FSW, residual stresses and distortions can be mitigated using a technique called thermal tensioning, which includes both heating and cooling methods. The heating approach involves applying a heat source, such as a laser or resistive heating blankets, to the weld zone before or during welding to locally increase the material's temperature. This localized heating reduces the yield strength of the material, making it more ductile and easier to deform without causing large residual stresses. By promoting more uniform deformation, this method helps minimize residual stresses and distortions (El-Moayed et al. [83]). On the other hand, cooling methods have been reported to significantly reduce residual stress during FSW. These methods typically involve directing a cooling source, such as liquid CO₂, water, or a mixture of compressed air and water, towards the tool during the FSW process. Studies by Staron et al. [88], Richards et al. [6], and Han et al. [89] have all reported significant reductions in residual stress due to this in-situ cooling (see Figure 30).

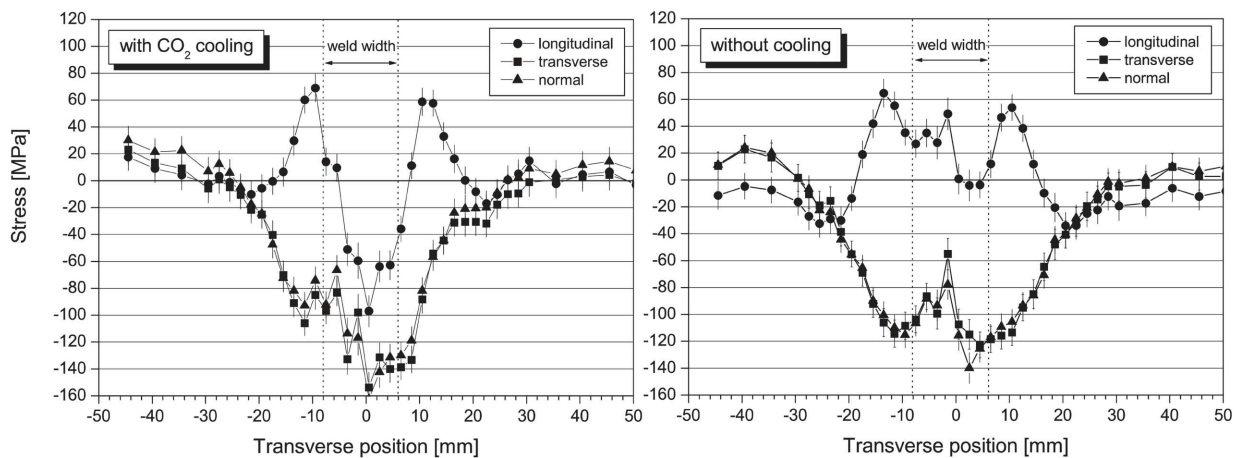


Figure 30: Residual stresses across AA 2024 T351 butt weld with and without CO₂ cooling (Staron et al. [88])

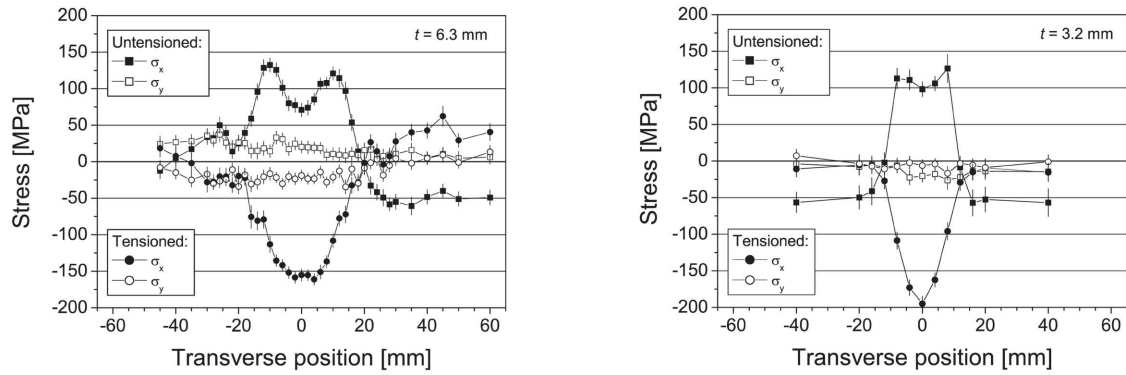
Thermal tensioning's efficacy in reducing residual stress stems from the cooling source's role as a heat sink, which diminishes the temperature gradient within the welded material. A large temperature gradient causes different sections of the material to expand and contract at non-uniform rates during heating and cooling, resulting in internal thermal stresses. By reducing these temperature gradients, thermal tensioning lessens the thermal stresses that arise during FSW, which is a key factor in generating residual stress. Notably, this reduction in residual stress occurs in the longitudinal direction surrounding the weld centerline (see Figure 30). This direction is

particularly significant as it is parallel to the weld and experiences the most substantial temperature changes and material flow during the FSW process. Various factors influence the effectiveness of thermal tensioning, including the size, power, and positioning of the cooling source (Richards et al. [6]). Additionally, the distance between the cooling source and the weld centerline, as well as the flow rate of the cooling medium, impact the extent to which residual stress is reduced. Therefore, optimizing temperature control, not only for active cooling but also for pre-heating surrounding areas in multi-welded components, could potentially improve residual stress management in welds.

2.3.4.2 Mechanical Tensioning

Mechanical tensioning is a technique employed to reduce residual stress in FSW by applying mechanical force to the workpiece. Staron et al. [90] describe this technique, which involves applying mechanical tensioning to aluminum sheets during the FSW process. The mechanical force is applied using a tensioning device, stretching the sheets to 70% of the tensile yield strength prior to welding. At this level, the material remains within its elastic range, meaning the tensioning itself does not permanently deform it. When the sheets are stretched to 70% of their yield strength, they undergo elastic deformation. The material returns to its original shape after the removal of the applied force, but during the welding process, the material's behavior can be influenced by this pre-applied stress. When the tension is released after welding, the material, which was elastically deformed, generates compressive stresses as it attempts to return to its original state (Staron et al. [90]). This generation of compressive stress occurs because the weld area, having been heated and deformed plastically during the welding process, is now in a state of outward expansion. When the tension applied during welding is released, the surrounding material, which was under elastic deformation, contracts back to its original dimensions. This contraction pulls against the plastically deformed weld area, creating compressive stresses. These compressive stresses counteract the tensile residual stresses produced during the FSW process. Staron et al. [90] observed significant changes in residual stress distribution. The sheets emerge from this modified welding process with large compressive stresses in the longitudinal direction, which is the direction in which the tensioning was applied during welding (see Figure 31). Similar findings have been reported by other researchers, demonstrating the effectiveness of mechanical tensioning in reducing tensile residual stress (Price et al. [77]; Altenkirch et al. [91]).

Roller tensioning, a variation of mechanical tensioning, involves using rollers to apply force either during (in situ roller-tension) or after (post-weld roller-tension) the FSW process. Altenkirch et al. [92] investigated the effects of roller tensioning on residual stress distribution by employing both in situ and post-weld roller tensioners. The results of both in situ and post-roller tensioning are shown in Figure 32, where in situ rolling had little effect while post-weld rolling had significant effects on the final residual stress distribution.



Residual stress results for the 6.3mm thick FSW AA2024 sheets. One sheet was welded untensioned, the other was tensioned to 70% tensile yield strength prior to welding.

Residual stress results for the 3.2mm thick FSW AA2024 sheets. One sheet was welded untensioned, the other was tensioned to 70% tensile yield strength prior to welding.

Figure 31: FSW joints under mechanical tensioning (Staron et al. [88])

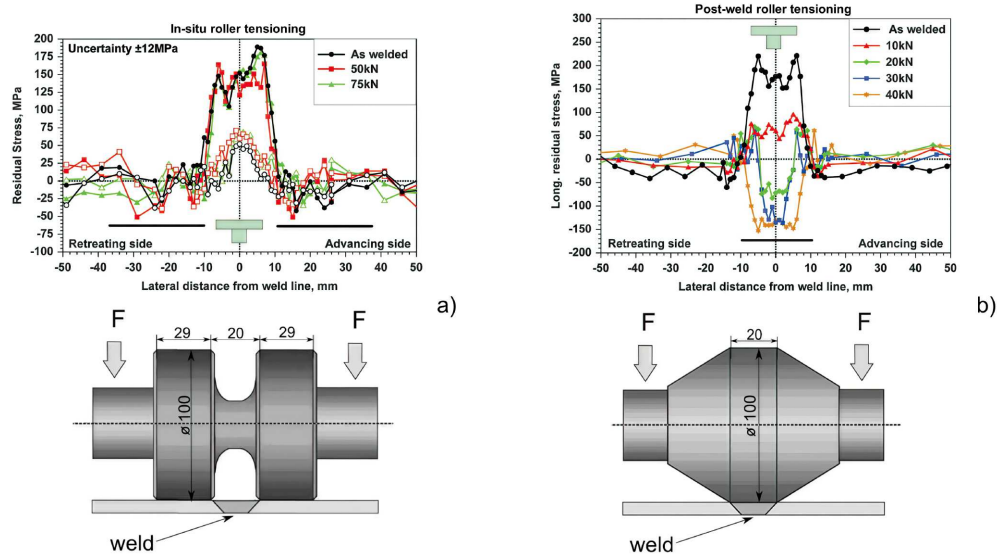


Figure 32: (a) Effect of in situ roller on the residual stress, solid lines longitudinal, open lines transverse; (b) Effect of post-weld roller on the residual stress of friction stir welds (Altenkirch et al. [92])

Another variation to mechanical tensioning, the coining operation utilized in this dissertation (see section “3.3.1.1 Post Weld Straightening”), is relatively specific to individual component geometries (i.e., battery tray). This operation requires specialized die tooling analogous to a coining press and a supportive structure below the component to deform the part and restore its geometry to the design specification (see Figure 33). Hence, the coining operation is similar to the post-weld global mechanical tensioning (PWGMT) method described by Altenkirch et al. [92]. PWGMT is a stress leveling technique requiring high tensioning loads that exceed the cold weldment yield strength to elongate the weld material plastically, which deforms the component to restore its geometry to the design specification.

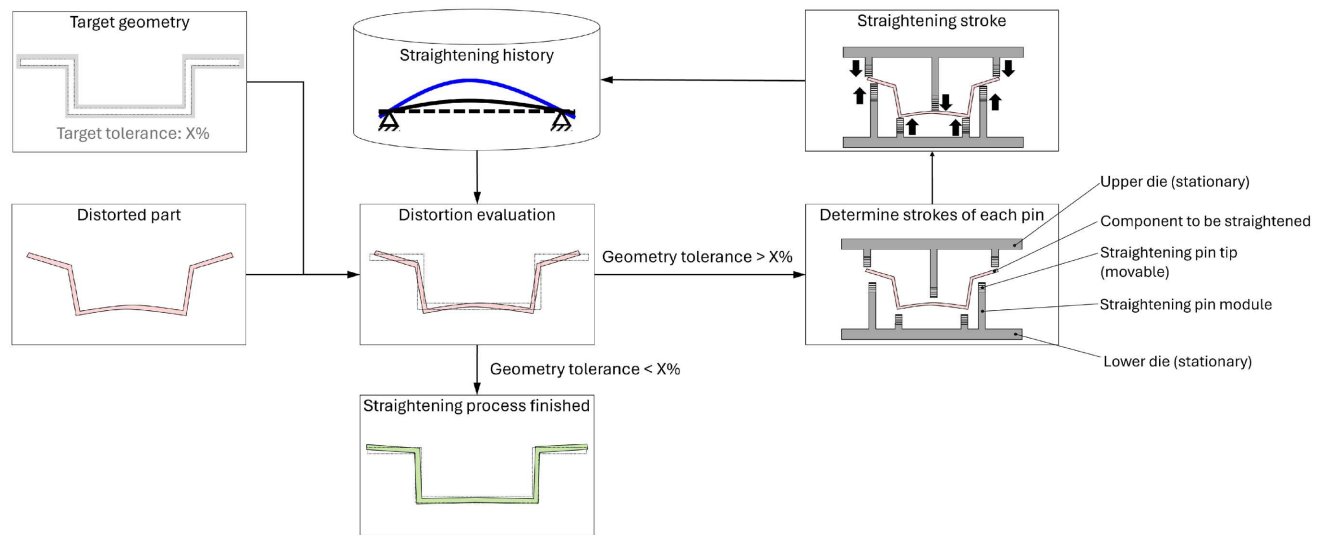


Figure 33: Schematic concept of the straightening coining operation (Park et al. [93])

PWGMT was specifically investigated by Price et al. [75], who referred to it as a “cold stretch” after welding. The cold stretch utilizing an applied stress corresponding to 80% of the material's yield strength was applied to panels 2024-T3 joined by FSW aluminum alloy. Price et al. [77] found the approach effective at removing weld distortions, specifically buckling. The post-weld cold stretch also had significant effects on residual stress (see Figure 34). The residual stress profile of the cold-stretched weld showed a much flatter distribution compared to an untensioned weld produced under identical conditions. The compressive stresses near the weld were reduced, falling between 210 and 250 MPa, which is below the critical buckling load for the plate's geometry. This flattening of the stress profile indicates a reduction in the residual compressive stresses that cause buckling.

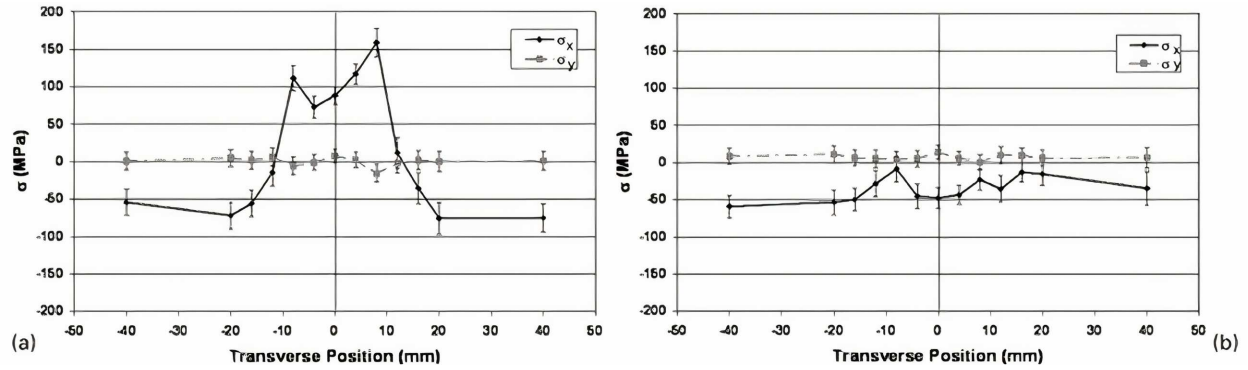


Figure 34: (a) Untensioned FSW; (b) FSW that was tensioned to 80% of YS after welding shows a flat residual stress profile (Price et al. [77])

The effects of mechanical tensioning on multi-welded components, which often experience excessive distortion, are not well-defined. These components frequently undergo operations aimed at reducing distortion by bending them back to pre-weld specifications, often without considering the potential increase in residual stress. A significant part of this dissertation focuses on exploring how far a component can be bent to achieve the desired dimensions without introducing additional undesirable residual stresses. Furthermore, it examines whether the plastic deformation resulting from such operations, if the applied stresses exceed the yield strength, could potentially release residual stress. This discussion is elaborated in detail in the section “3.3 Analyzing Distortion and Stress Effects in Response to Straightening the Battery Tray”.

2.3.4.3 Surface Treatment

Surface treatment is a common method for reducing residual stress in FSW. Among those treatments that exist, burnishing is a surface treatment method that involves the use of a hard, smooth tool to apply pressure and friction to the surface of a material. Burnishing is a technique that employs a hard tool, such as a ball or roller, to press against a metal surface, compacting and smoothing it. This method enhances the surface finish of the workpiece and can also increase its microhardness. It's a beneficial finishing process that doesn't remove material from the workpiece's surface. The burnishing process introduces compressive residual stress due to the effect of plastic deformation. When a burnishing tool is applied to a material's surface with sufficient force to cause plastic deformation, it smooths out the high points of the metal surface into the low points. This action results in a uniform surface and introduces compressive residual stress. The compressive residual stress induced by burnishing can enhance the fatigue life of the treated part, delaying the onset of cracks on the surface (Başak et al. [94]; Kluz et al. [95]; Prev  y

et al. [96]). In the case of FSW joints, burnishing can be used as an effective means of minimizing residual stress generation (Kluz et al. [95]). It is known that due to the high heat and mechanical deformation experienced by the material during the FSW process, tensile residual stresses are introduced in and around the weld zone. To counteract this, burnishing introduces compressive stresses into the surface layer of materials through plastic deformation caused by sliding contact between a hard tool and material surfaces (see Figure 35). This process continuously indents the weld surfaces, causing localized cold working and compressive residual stress formation beneath these indentations. These indentations have been shown to significantly reduce both longitudinal and transverse tensile residuals while increasing microhardness values compared with non-burnished samples (Rodríguez et al. [97]).

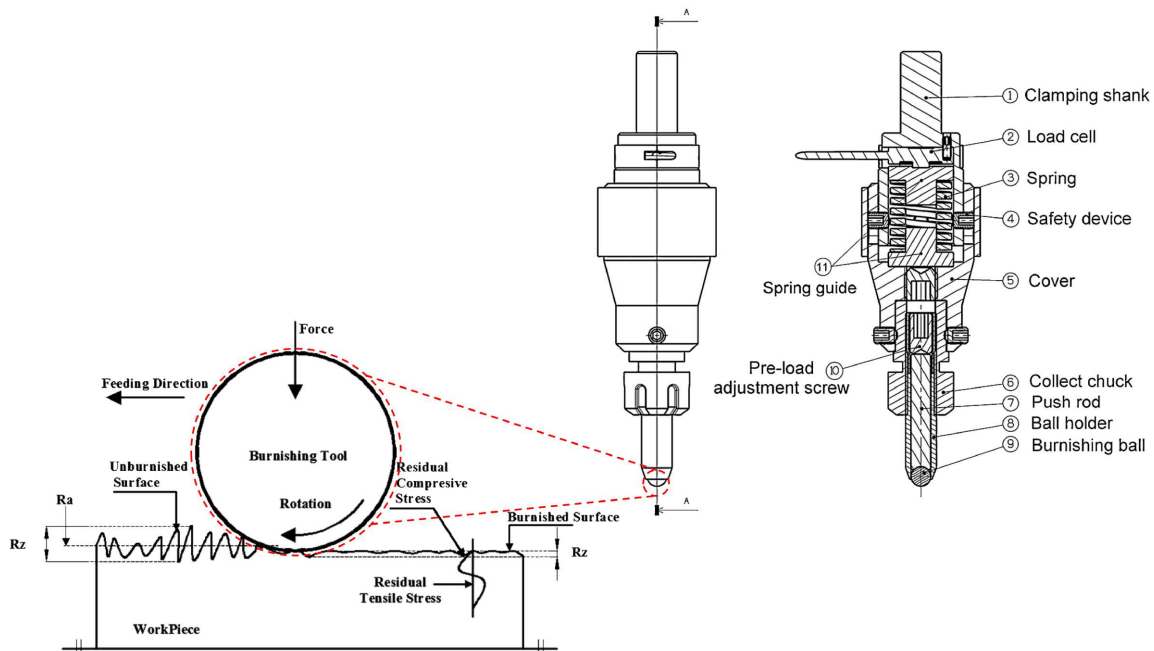


Figure 35: Schematic working principle of burnishing process (Ovali et al. [98]) and burnishing tool design (Shiou et al. [99])

As a positive consequence, the weld's fatigue life increases, reducing the risk of premature failure in welded structures. However, it is worth noting that burnishing is not a one-size-fits-all solution for minimizing residual stress generation during FSW joints. The optimal burnishing parameters will depend on material properties, joint geometry, tool design, and process conditions. Therefore, it is important to carefully evaluate the effects of different burnishing parameters on residual stress formation before implementing this technique into industrial applications. Other surface treatment options include laser and shot peening, which utilizes high-energy laser and shot particles,

respectively, to bombard the material's surface. Hatamleh et al. [100] found that, similarly to burnishing, this process causes the surface to undergo plastic deformation, introducing compressive stresses that counteract the tensile stresses generated during welding (see Figure 36).

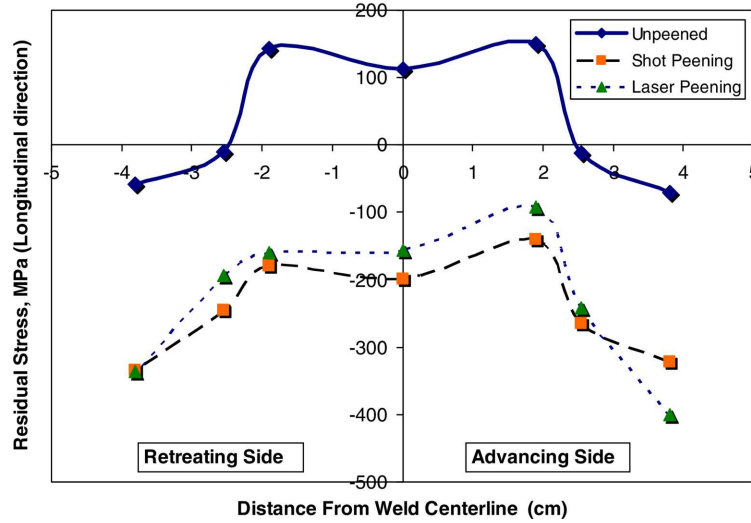


Figure 36: Residual stresses for the various peened AA 2195 joints FSW specimens (Hatamleh et al. [100])

Although burnishing is widely used and frequently referenced in literature for friction stir welds, precise documentation of its effects on both deformation and residual stress in multi-welded components is sparse. This dissertation compares the burnishing operation with the coining operation in reducing, eliminating, or mitigating distortion and residual stress. The study also examines how these residual stresses redistribute in various directions. For a detailed explanation, please refer to section “3.3.4 Residual Stress after Burnishing and Coining”.

2.3.5 Section Summary

FSW introduces a unique residual stress profile that differs from traditional fusion welding, largely due to its solid-state nature and the mechanical stirring involved. As highlighted, FSW generally results in an 'M-shaped' residual stress profile with localized peaks. This unique profile contributes to reduced distortion and lower residual stresses compared to traditional welding methods because mechanical stirring promotes more uniform thermal distribution and plastic deformation, reducing the likelihood of high-stress concentrations. However, existing literature predominantly focuses on single weld scenarios, leaving a knowledge gap in understanding residual stresses in industrial components with multiple welds in proximity to each other. This dissertation helps to address this gap by exploring the cumulative effects of multiple welds on residual stress and distortion,

particularly in industrial components (in this case, a battery tray), as well as in simplified geometries such as plates used for multi-welded analysis. Additionally, the dissertation investigates the efficacy of post-weld treatments such as mechanical coining and burnishing in modifying residual stresses and enhancing component geometry, specifically in the battery tray, the multi-welded industrial component. Through a study of the interactions between residual stresses and post-weld treatments, this research contributes to a deeper understanding of FSW, supporting its broader application in manufacturing complex, high-integrity components.

From a more fundamental perspective, although still industrially connected, as manufacturing sectors seek to weld at faster and faster speeds while maintaining manageable residual stress, there are still aspects to understand. The dissertation conducts single-welded analysis at increasing welding speeds with a constant speed ratio to examine the effects of texture on residual stress, primarily through its influence on yield strength. Texture influences yield strength, which, in turn, limits the total residual stress. Mechanisms that actually generate residual stress, such as high-temperature gradients, are supported through Smoothed Particle Hydrodynamics (SPH) simulations, given this dissertation's unprecedented view in combining residual stress, texture, and temperature measurements. This dual approach highlights both industrial applications and fundamental insights into residual stress mechanisms.

Thus, the subsequent sections of the literature review will elaborate on the principal methods for measuring residual stress and texture. Supporting experiments, such as distortion measurement with coordinate measuring machines, tensile testing, ultrasonic analysis, and PiP analysis, are briefly introduced and reviewed before being utilized in their respective results sections.

2.4 Methods for Measuring Residual Stress in Friction-Stir-Welding

In this study, it is important to explore relevant techniques employed for residual stress determination and address practical issues associated with its measurement to select the most appropriate method. Residual stresses are more challenging to predict than the in-service stresses they superimpose, necessitating reliable measurement methods and understanding the extent of information they can offer. It is fundamental to recognize that stress arises from elastic strain, which is independent of plastic deformation, as residual stress is no longer present when the material undergoes complete and uniform plastic deformation. However, in real-world scenarios,

plastic deformation is non-uniform, meaning that different regions of the material deform to different extents. This non-uniform deformation occurs due to various factors such as differences in temperature, material properties, or applied forces during processes like welding. As a result, while some parts of the material may undergo significant plastic deformation, other parts may remain largely elastic. Due to this non-uniformity, materials consistently contain elastic components. These elastic components are areas of the material that have deformed but not to the point of permanent plastic deformation. Instead, they have deformed elastically, meaning they can return to their original shape when the applied stress is removed. These elastic regions contribute to the overall residual stress within the material. Residual stresses arise because the material tries to return to its original shape after the external forces are removed, but the presence of plastically deformed areas prevents this, creating internal stresses. These residual stresses are measurable as elastic strain, which is the deformation that remains in the material after the external forces are no longer applied. Most methods for determining residual stresses are considered indirect because they involve measuring a related property (such as strain or material deformation) and using that information to infer the residual stress. Due to the inherent nature of residual stress as a “locked-in” property, the direct measurement of residual stresses is not entirely possible; some methods are just more direct than others.

2.4.1 Hole-Drilling Method

The hole-drilling method is a destructive technique for assessing residual stress in various materials, including those used in FSW (Javadi et al. [101]; Ya et al. [102]; Dattoma et al. [103]). When a small hole is drilled into a stressed body, the local stresses around the hole are relieved, leading to stress redistribution in the nearby region. This occurs because the surface of the hole, acting as a new free surface, nullifies both shear and normal stresses (Mathar et al. [104]). The hole-drilling method measures specific strain components (perpendicular and shear), which are then used to estimate the transverse and longitudinal residual stresses. This approach provides useful information about the stress state in two directions but has limitations in fully capturing the vertical stress component (Bandyopadhyay et al. [105]). Vertical stresses refer to the stress components that act through the thickness of the material, perpendicular to the plane of the material's surface. These stresses that the hole-drilling method can measure are generally

accounted for by measuring the resulting strain changes on the surface of the test specimen using a strain gauge rosette, which can have either three or six elements (refer to Figure 37).

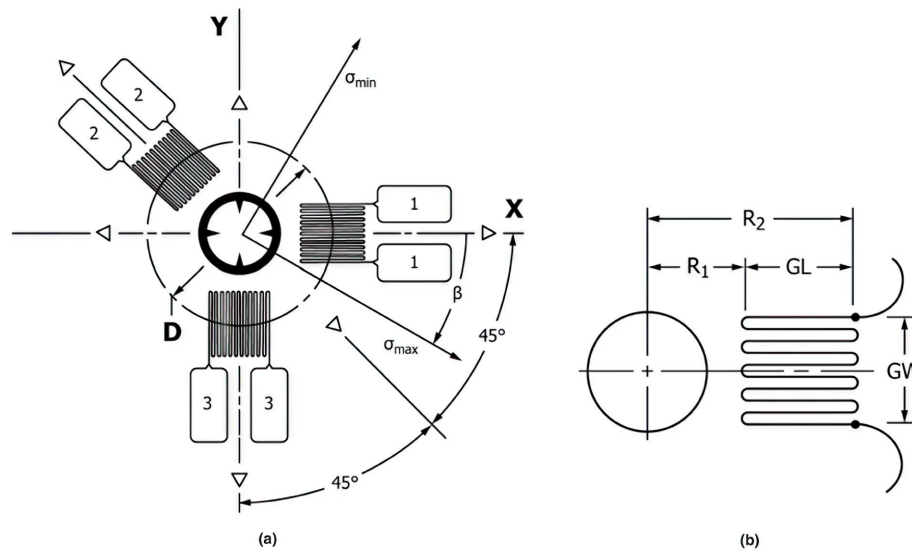


Figure 37: 2 Schematic geometry of a typical three-element clockwise hole-drilling rosette; (a) Rosette Layout; (b) Detail of a strain gage (ASTM E837 [106])

This setup allows for an indirect assessment of the stress components in different directions by analyzing the strain responses at various orientations around the hole. Residual stress values can be derived from these strain measurements through specific calculations and formulas that incorporate experimental data and finite element analysis (TN-503 [107]). The hole-drilling method is cost-effective and quick to execute. Despite its advantages, the hole-drilling method has certain limitations. These include limited sensitivity to strain, potential errors and uncertainties associated with hole dimensions (such as diameter, concentricity, profile, and depth), and issues related to surface preparation, including roughness and flatness. Interpreting the data can also be complex, and the technique is inherently destructive, permanently altering the tested component. Incremental hole drilling, however, enhances the method's flexibility, enabling stress profile and gradient measurements. A variant known as deep hole-drilling method has been developed specifically for assessing residual stresses in thick-section materials. This involves creating a small reference hole and then removing a column of material around it using a trepanning technique, which refers to the process of cutting a circular groove around the hole to isolate a cylindrical core of material. Changes in the dimensions of the reference hole and the material column after stress relaxation provide the basis for calculating the residual stresses (Schajer et al. [108]).

When it comes to measuring residual stresses in complex aluminum parts that have undergone FSW, the hole-drilling method might not be the most effective approach. For example, the stress fields in these parts are often non-uniform because of the extensive plastic deformation involved in the welding process. Second, the method involves drilling into the material, which can cause localized damage. This might not be a concern for a single measurement, but when multiple locations need to be probed, the cumulative damage could affect the part's integrity. Third, the microstructure of the welded aluminum is typically complex, which could potentially compromise the accuracy of the measurements. Lastly, the method requires surface access, which might not be feasible for all areas of interest in complex parts. Therefore, while the hole-drilling method has its merits, its limitations should be considered when dealing with complex aluminum friction stir welded parts. Other stress measurement techniques are more suitable in such cases.

2.4.2 X-Ray Diffraction

X-ray diffraction (XRD) is a non-destructive method employed to measure residual stresses in materials. This technique is based on the elastic deformation of the crystal lattice induced by residual stresses, resulting in altered lattice plane spacing compared to stress-free conditions. XRD involves projecting a beam of X-rays onto the material's surface and examining the diffraction pattern created by scattered X-rays (Chang et al. [109]). This pattern is then analyzed to deduce the residual stress present in the material by strain through the measurement of the interplanar spacing by Bragg's law, which will be discussed further in section "2.4.3.1 Bragg Diffraction". XRD boasts several advantages, including its widespread availability due to its established role in measuring residual stress across various materials. It also offers high spatial resolution, ranging from 1-2 mm to tens of microns, and a penetration depth of approximately 10-30 microns, depending on the material and source. These attributes make XRD suitable for assessing residual stresses in numerous materials, such as those found utilized in FSW (Zapata et al. [110]; Prime et al. [111]). As a non-destructive method, XRD allows for in-situ residual stress measurements without damaging the material.

However, XRD also presents some limitations. The ability of X-rays to penetrate substances is limited due to their interactions with the atomic structure of the material. When X-rays come into contact with a substance, they engage primarily with the material's electrons. Some X-ray photons

will interact with atoms, losing their energy in the process, while others may traverse the material with little to no interaction (Cullity et al. [112]). Their energy levels also influence the penetration depth of X-rays. X-rays with higher energy (and consequently shorter wavelengths) possess more energy that needs to be absorbed, allowing them to penetrate deeper into the material. This is because the energy of an X-ray photon is inversely proportional to its wavelength. Higher energy (shorter wavelength) X-rays can overcome the atomic and electronic interactions more effectively, enabling deeper penetration (Cullity et al. [112]). Nonetheless, even these high-energy X-rays will expend some of their energy as they traverse materials. For instance, certain metals can absorb a portion or all of the energy from the X-rays (Cullity et al. [112]). The absorption of X-rays by a material is closely tied to the X-ray cross-section of the material, which is a measure of the likelihood that an X-ray photon will interact with an atom in the material. When an X-ray photon encounters an atom, it can be absorbed, scattered, or pass through without interaction. The probability of each of these events is determined by the cross-section. A larger cross-section implies a larger “target” for the X-ray photon, increasing the chance of an interaction (Warren et al. [113]). For example, if aluminum has a relatively large X-ray cross-section, it is more likely for X-ray photons to interact with aluminum atoms (see Figure 40). This interaction can lead to the absorption of the X-ray photon’s energy. Due to this, X-rays have limited penetration power (more so in some materials than others); therefore, analyzing residual stresses deep within the target material (i.e., aluminum) is not possible with X-rays.

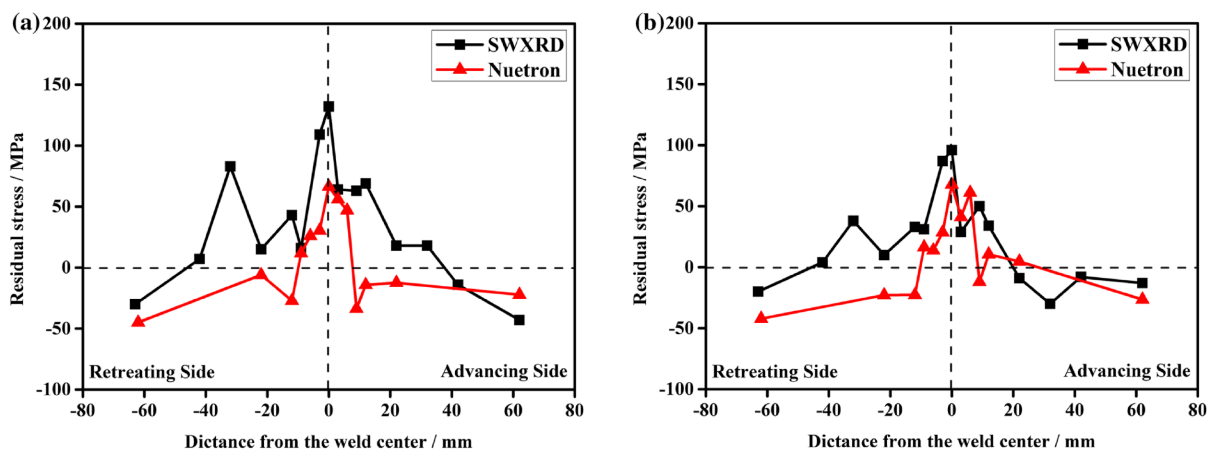


Figure 38: Residual stress profile comparison tested by short wavelength X-ray diffraction (SWXRD) and neutron diffraction in (a) Transverse direction; (b) Longitudinal direction (Ji et al. [114])

Moreover, certain researchers, including Prime et al. [111], have mentioned that X-ray investigations have revealed a degree of uncertainty in aluminum FSW. Referring to uncertainties that would have complicated the process of obtaining meaningful measurements from the specimen analyzed in their study. As a result, they chose to employ a combination of neutron diffraction and other measurement strategies for their residual stress assessment. Moreover, the entire component must fit within the diffractometer, which can be problematic for larger pieces. Ji et al. [114] conducted a comparison study between short-wavelength X-ray diffraction and neutron diffraction, finding similar differences (see Figure 38).

2.4.3 Neutron Diffraction

Neutron diffraction is a method that utilizes elastic deformations in polycrystalline materials to assess residual stress. This approach is akin to X-ray diffraction, in which a detector revolves around the sample to identify high-intensity diffracted beams' positions. The primary advantage of neutrons over X-rays is their significantly better penetration depths, enabling measurements from near-surface depths of approximately 0.2 mm up to bulk depths of 300 mm in aluminum or 25 mm in steel (Hutchings et al. [115]; Pynn et al. [116]; Kandil et al. [117]). This penetration difference is due to the fact that X-rays are electromagnetic radiation, and they interact with the charged cloud of electrons around the atom's nucleus. Neutrons, on the other hand, are electrically neutral and interact directly with the nucleus of the atom. This leads to a difference in the cross-section of interaction between the two, where the cross-section of interaction determines how likely the respective particle will hit an atom, leading to a larger penetration depth by neutrons for most materials (see Figure 39). For example, an X-ray cross-section is essentially a proportional relationship to the number of electrons in the atom (see Figure 39). Such elements, like hydrogen, have very low cross-sectional areas compared to elements like iron for X-rays (see Figure 40). On the other hand, neutrons' scattering length or cross-section of interaction varies chaotically between elements because of a complex relationship between many factors; even different isotopes of the same element can have very different neutron cross-sections. This chemical sensitivity is a crucial aspect of neutron diffraction that cannot be achieved with X-rays. Furthermore, since elements with many electrons, such as aluminum and iron, have a large cross-sectional area for X-rays, they have a relatively small cross-sectional area for neutrons. This quality allows neutrons to penetrate deep into metal or non-transparent objects, whereas X-rays cannot penetrate as deeply into non-

transparent objects due to their photon nature. Furthermore, facilities like the High-Intensity Diffractometer for Residual Stress Analysis at Oak Ridge National Laboratory and the Kowari stress spectrometer at the Australian Nuclear Science and Technology Organisation (facilities utilized in this study) are capable of handling large engineering components, enabling the creation of comprehensive three-dimensional stress maps.

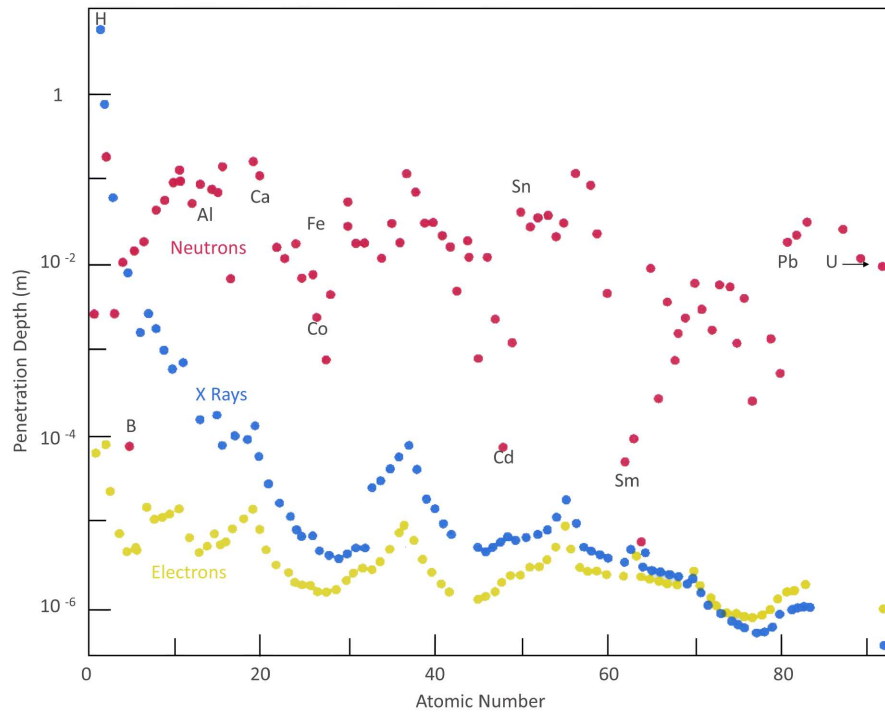


Figure 39: Comparison of depth penetration between a beam of electrons, x-rays, or thermal neutrons for a particular element in its solid or liquid form. The neutron data are for neutrons having a wavelength of 1.4 angstroms (Pynn et al. [116])

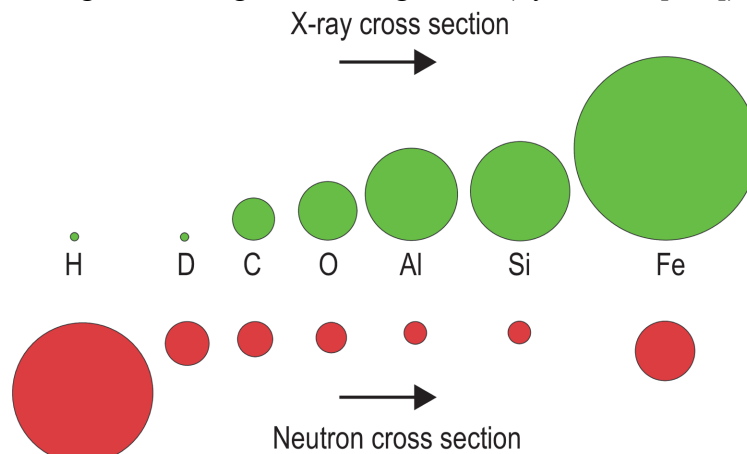


Figure 40: Neutrons and X-ray scattering cross-sections compared. Note that neutrons penetrate through aluminum much better than X-rays do, yet are strongly scattered by hydrogen (Jacobson et al. [118])

Neutron diffraction's ability to collect vast amounts of data across entire surfaces and depths has proven invaluable for validating theoretical and numerical models. In addition to its research applications, neutron diffraction is employed commercially to optimize new joining processes by minimizing residual stress development. Despite its numerous benefits, neutron diffraction has some drawbacks when compared to other techniques, such as X-ray diffraction. The primary challenges include higher costs and limited availability, which are attributed to the difficulties in generating sufficient neutron flux and the requirement of large-scale facilities. Currently, neutron diffraction is conducted at centralized facilities, such as ISIS (Didcot, England), ILL (Grenoble, France), Saclay (Paris, France), ORNL (Oak Ridge, United States), LANL (Los Alamos, United States), and ANSTO (Australia, Lucas Heights). Beam-time access is typically granted through a competitive peer-review process, often necessitating instrument reservations several months in advance. To summarize, while neutron diffraction is a powerful tool for measuring residual stress in polycrystalline materials, factors such as cost, availability, and limitations in shape complexity must be considered. Nevertheless, its advantages outweigh these limitations, making it a key method used in this study for residual stress measurement. The following sections will provide a brief overview of the physical principles underlying neutron diffraction and the specific techniques applied to calculate residual stress.

2.4.3.1 Bragg Diffraction and Laue Equations

Bragg diffraction is a cornerstone principle in materials science and is critically required to measure residual stress. This principle revolves around the constructive interference of waves scattered by a crystal lattice. For example, neutron diffraction occurs when a beam of neutrons with a wavelength that matches the distance between atoms in a crystal lattice interacts with that lattice. The diffracted neutrons interfere with each other constructively or destructively. Neutrons can behave like waves and exhibit different phase shifts, causing them to either amplify or cancel out their amplitudes. This interference is particularly relevant in diffraction when waves traveling different path lengths can fall out of phase even if they possess the same wavelength, resulting in destructive interference (see Figure 41).

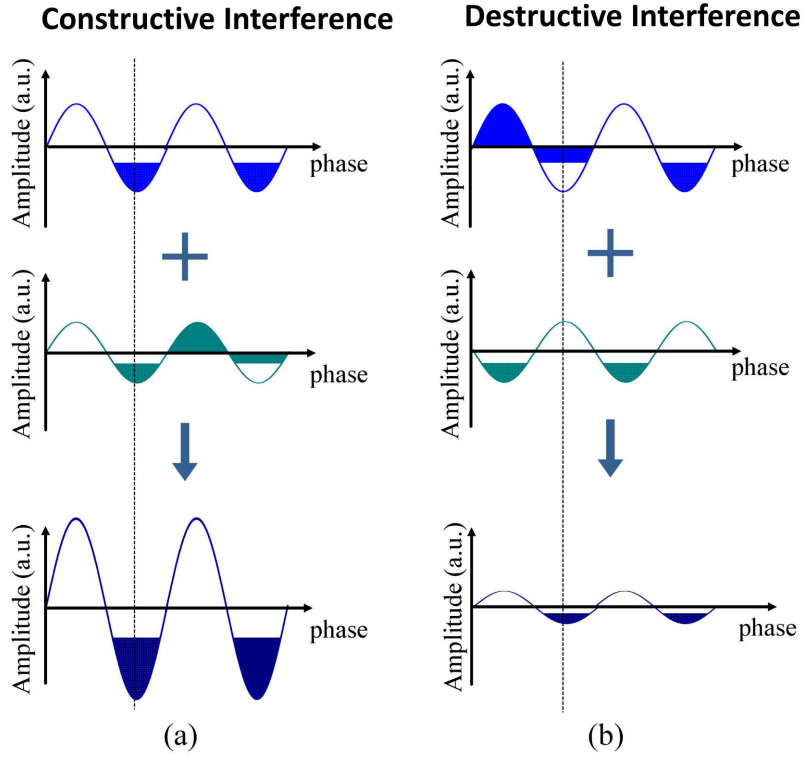


Figure 41: Schematic diagram of constructive interference and destructive interference (Gao et al. [119])

These interactions are significant as only specific angles lead to fully constructive interference, which results in a strong signal. When millions of neutrons shot from the beam are involved, even small deviations from the fully constructive condition can cause significant cancellation. However, when the Bragg condition is met, constructive interference occurs, resulting in the amplification of waves and a diffraction peak from the counted neutrons. The angle at which this peak appears is linked to the spacing between atoms in the lattice and the incident beam's wavelength. According to Bragg's law, if the total path length difference, calculated in Equation 4, equals an integer multiple (n) of the wavelength (λ), constructive interference occurs (see Figure 42), meaning the waves do not cancel each other out, but instead amplify the signal (Als-Nielsen et al. [120]).

$$n\lambda_{hkl} = 2d_{hkl} \sin \theta \quad \text{Equation 4}$$

This results in neutron counts at specific angles (θ), where constructive interference is maintained for a given interplanar distance (d). In contrast, other angles lead to a combination of constructive and destructive interference, reducing the number of neutrons reaching the detector and resulting

in the bell-shaped effect moving away from the angle that caused constructive interference in diffraction patterns (see Figure 44).

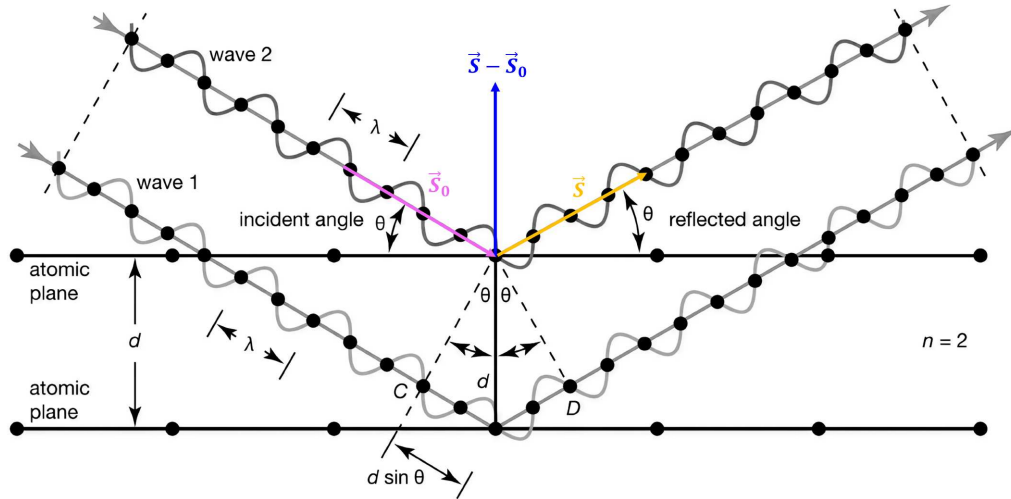


Figure 42: Bragg diffraction (Britannica [121])

Laue's equations, on the other hand, are a set of three equations that describe the relationship between the reciprocal lattice vectors and the real-space lattice vectors. They are used to determine the angles at which constructive interference occurs in a three-dimensional crystal. They describe the conditions under which neutrons are diffracted by a crystal, resulting in constructive interference. In one-dimensional diffraction patterns, the spacing between diffraction lines is inversely related to the distance between atoms, which defines the reciprocal lattice space. Adding another dimension requires solving Laue's equations for that direction, generating a grid of diffraction points instead of lines. When solving Laue's equations for all three dimensions, constructive interference occurs where all three lines or cones intersect, known as a Debye-Scherrer cone. This cone shows at which specific angles the incident neutron diffracts constructively, creating a series of diffraction spots in a conical shape (see

Figure 43). Laue's equations in the context of neutron diffraction represent the three dimensions h , k , and l , known as Miller indices, which in turn denote the (hkl) planes in a crystal lattice (see Equation 5). Each dimension corresponds to one of the three crystallographic axes. For constructive interference to occur in three dimensions and produce a diffraction peak, all three of Laue's equations must be satisfied simultaneously. Laue's equations are often used for indexing diffraction patterns and determining the crystal orientation in a 3D material using interplanar spacing formulas; Equation 6 exhibits the cubic system interplanar spacing formula for aluminum

FCC derived from Equation 4 and Equation 5. However, for calculating strain, Bragg's Law is sufficient because the specific set of (hkl) planes for which constructive interference occurs is already known before diffraction. Therefore, although not explicitly utilized in the calculation, the (hkl) planes from Laue's equations are inherently considered. For instance, utilizing the diffraction pattern of aluminum in Figure 44, along with the wavelength ($\lambda = 1.7232 \text{ \AA}$), peak center ($\theta_{hkl} = 89^\circ$), and lattice parameter of FCC ($4.05 \text{ \AA}$), the hkl plane of the peak can be indexed. Hence, from the result of Equation 7, the diffraction peak occurring in Figure 44 is the $\{311\}$ plane.

$$\begin{aligned} h\lambda &= \vec{a} \cdot (\vec{S} - \vec{S}_0) \\ k\lambda &= \vec{b} \cdot (\vec{S} - \vec{S}_0) \\ l\lambda &= \vec{c} \cdot (\vec{S} - \vec{S}_0) \end{aligned} \quad \text{Equation 5}$$

$$\frac{1}{d_{hkl}} = \frac{h^2 + k^2 + l^2}{a^2} \quad \text{Equation 6}$$

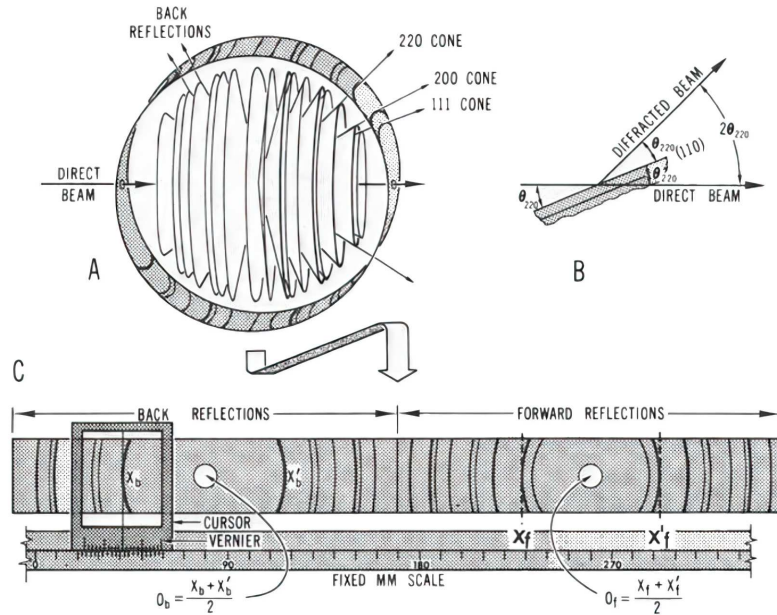


Figure 43: This schematic shows the Debye cones that intersect the film in the camera (Connolly et al. [122])

$$\theta \approx (89^\circ)/2 = 44.5^\circ \quad \text{Equation 7}$$

$$n\lambda_{hkl} = 2d_{hkl} \sin \theta, n = 1$$

$$1.7232 \text{ \AA} = 2d_{hkl} \sin 44.25^\circ$$

$$d_{hkl} = \frac{1.7232 \text{ \AA}}{2 \sin 44.5^\circ} = 1.2293 \text{ \AA}$$

$$d_{hkl} = \frac{a^2}{h^2 + k^2 + l^2}, a = 4.05 \text{ \AA}$$

$$h^2 + k^2 + l^2 = \left(\frac{4.05 \text{ \AA}}{1.2293 \text{ \AA}} \right)^2 = 10.854 \approx 11$$

$$h^2 + k^2 + l^2 = (3)^2 + (1)^2 + (1)^2 = 11$$

Miller indices = {311}

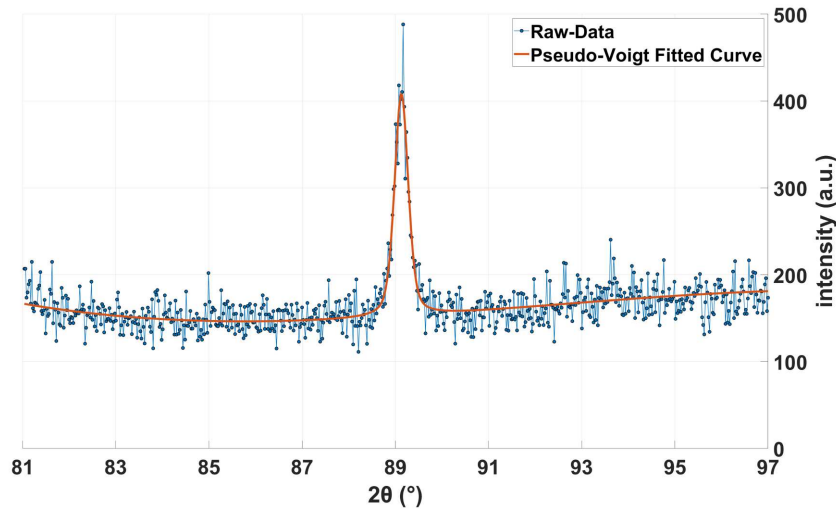


Figure 44: Example diffraction peak from counting neutrons

It is important for a diffraction experiment focused on Type 1 stress to select a diffraction plane unaffected by intergranular strains. Intergranular strains originate from stress concentrations at the boundaries of crystalline grains, which can introduce errors in residual stress measurements and lead to inaccurate results. In aluminum alloys, the {311} plane is commonly utilized for this purpose. The {311} plane exhibits a relative insensitivity to intergranular strains, making it a reliable choice for obtaining precise residual stress measurements (ISO/TS 21432:2005 [123]). Intergranular strains can impact residual stress results when inappropriate diffraction planes are used because these strains cause local variations in lattice parameters and atomic positions within the crystal lattice. When a diffraction plane is sensitive to intergranular strains, these variations can alter the expected peak positions and intensities in the diffraction pattern. Consequently, the resulting measurements may not accurately represent the true residual stress within the material. Instead, they may reflect a combination of the intrinsic residual stress and the effects of intergranular strains, leading to a distorted understanding of the material's properties. The

influence of intergranular strains on residual stress measurements can be minimized by selecting appropriate diffraction planes, such as the $\{311\}$ plane in aluminum alloys. This ensures more accurate results and a better understanding of the material's behavior under various conditions. The following section, “2.4.3.2 The Peak-Shift Method in the Calculation of Residual Stresses”, will provide an example of the differences in peak behavior caused by residual stresses that are utilized to calculate strain and then stress.

2.4.3.2 The Peak-Shift Method in the Calculation of Residual Stresses

After establishing the fundamentals of Bragg's law and Laue's equations in diffraction and atomic plane indexing, the peak-shift method in calculating residual stresses is now discussed. The peak shift method builds on the concepts introduced earlier, utilizing specific diffraction peaks and their positions to determine the stress experienced by the material. The peak shift method identifies and quantifies the differences in peak positions between a stress-free material (d_0) and a stressed material (d) in the diffraction patterns. Comparing these peaks allows for the calculation of the strain (ε) experienced by the material. This method is particularly effective in conjunction with neutron diffraction, as it accurately determines d -spacing in specific component directions. The specific methods reviewed to acquire strain were initially applied by Sediako et al. [16], employing the equations established by Hutchings et al. [115] to determine the stress in all three orientations (longitudinal-X, transverse-Y, and normal-Z). Furthermore, the breakthrough work by Schajer et al. [124] formulated in detail the method of experimental elastic strains and stresses in terms of lattice interplanar spacing measurements. This section utilizes an example segment of friction stir weld on an aluminum battery tray to demonstrate the processes of these methods to determine residual stress. According to Bragg's law, when the $\{311\}$ atomic planes of the aluminum battery tray diffract the incident monochromatic neutron beam with a wavelength of $\lambda = 1.7232 \text{ \AA}$, a diffraction pattern with a peak at an angle $2\theta_{hkl}$ can be observed. For thermal neutrons, the $\{311\}$ reflection typically occurs in the range of 75-100 degrees of 2θ . Ideally, researchers aim for a 90-degree angle to simplify the gauge geometry and data processing. For pure aluminum, this diffraction typically exists at a wavelength of $1.727 \text{ \AA}$. Given the differences in the alloy, a wavelength of $\lambda = 1.7232 \text{ \AA}$ was selected, placing the beam at approximately 90° , depending on the strain experienced by the $\{311\}$ atomic planes. As mentioned in the section “2.4.3.1 Bragg Diffraction and Laue Equations,” the $\{311\}$ planes are unaffected by the material's intergranular

stress; therefore, only macro-scale stress from the manufacturing process is detected. After pattern collection, Bragg's law relates the θ_{hkl} obtained from the diffraction pattern and the wavelength λ to the position of each d_{hkl} plane, otherwise known as d -spacing, which is the distance between parallel planes of atoms. Bragg's Law (Equation 4) incorporates the following symbols: n for the atomic order, λ for the wavelength, d_{hkl} for the d -spacing, and θ_{hkl} for the diffraction angle. Neutron diffraction is utilized to obtain the d -spacing in a specific component direction. The measured component direction is identified by the scattering vector ($S-S_0$), which is the subtraction of the diffracted vector (S_0) and the incident vector (S). The scattering vector is perpendicular to the measured plane. The sample can then be rotated by 90 degrees to obtain the strain value measured in the next direction for the same location. From the d -spacing measurements, strain can be calculated with Equation 8. As shown in Equation 8, a stress-free (d_0) reference is required as a baseline to quantify the strain (ε) in the stressed (d) material.

Consequently, two individual diffraction scans are required: one for the stressed material and one for the stress-free material. After obtaining the angular positions of the peaks for both conditions, the d -spacing of the stressed (d) and stress-free (d_0) material is determined using Bragg's Law (Equation 4). The strain experienced by the material is then calculated using Equation 8. Measuring the positions of the diffracted peaks for the stressed (d) and stress-free (d_0) material and determining the difference in peak positions to calculate strain (ε) is known as the peak shift method. This process is completed for strain in three orientations (longitudinal-X, transverse-Y,

$$\varepsilon = \frac{d - d_0}{d_0} \quad \text{Equation 8}$$

and normal-Z), allowing the calculation of stress in each orientation using Equation 9 (Noyan et al. [125]). In this equation, σ_{xyz} represents the stress for each orthogonal direction in MPa, E is the modulus of elasticity in GPa , ν is Poisson's ratio, and ε_{xyz} is the strain for each direction.

$$\begin{aligned} \sigma_x &= \frac{E}{(1 + \nu)(1 - 2\nu)} [(1 - \nu)\varepsilon_x + \nu(\varepsilon_y + \varepsilon_z)] \\ \sigma_y &= \frac{E}{(1 + \nu)(1 - 2\nu)} [(1 - \nu)\varepsilon_y + \nu(\varepsilon_x + \varepsilon_z)] \\ \sigma_z &= \frac{E}{(1 + \nu)(1 - 2\nu)} [(1 - \nu)\varepsilon_z + \nu(\varepsilon_x + \varepsilon_y)] \end{aligned} \quad \text{Equation 9}$$

The multiplier term outside of Equation 9 adjusts the modulus of elasticity by Poisson's ratio, accounting for the material's overall response to stress. The first term inside the brackets of

Equation 9 represents the direct strain in the x, y, or z direction, contributing directly to the stress in the corresponding direction. The second term inside the bracket accounts for the influence of the sum of the strains in all three directions (x, y, and z) due to Poisson's effect, reflecting the material's tendency to contract or expand in perpendicular directions when stretched or compressed. Thus, the total stress in any given direction is influenced by both the strain in that specific direction and the combined effect of strains in all three directions. Below is an overview of the peak shift method procedure utilized in this dissertation, starting with Figure 45a, displaying the location of a scan line in the middle of the battery tray. The following image includes the diffraction pattern of each scan point along the example scan line (see Figure 45). First, the diffraction patterns must be fitted to obtain the peak center angle (see Figure 45). The fitting is done with a pseudo-Voigt function as the line profile for the diffraction patterns. The pseudo-Voigt function is a linear combination of Gaussian and Lorentzian functions and is the most common line-fitting function for neutron diffraction data (Pecharsky et al. [126]). As mentioned earlier, a stress-free reference is required to determine strain by comparing the shifting peaks between the stress-free and stressed patterns. Figure 45c contains the diffraction patterns, with the stress-free pattern situated above the stressed pattern. The stress-free reference is positioned above the stressed pattern due to its different background intensity. Therefore, the backgrounds are removed to visually compare the shifting peaks, as shown in Figure 46. With the pseudo-Voigt fitted peaks, each peak center angle can be obtained and converted into the distance between parallel planes of atoms (d -spacing) using Bragg's law (Equation 4), given that θ_{hkl} is half of 2θ . Following Bragg's calculation, the d -spacing is converted to strain using Equation 7, as shown in Figure 46. Since the stressed peak is shifted to the left of the stress-free peak, the resulting strain is tensile, as indicated at the end of the calculation in Figure 46. For comparison, the resulting strain would have been compressive if the stressed peak had shifted to the right of the stress-free peak. The strain is then calculated for the remaining directions (including normal), and using Equation 9, the residual stress can be determined. As defined by the method just described, this dissertation does not adopt the plane stress assumption method, which assumes that the residual stress in the plate's normal direction is zero.

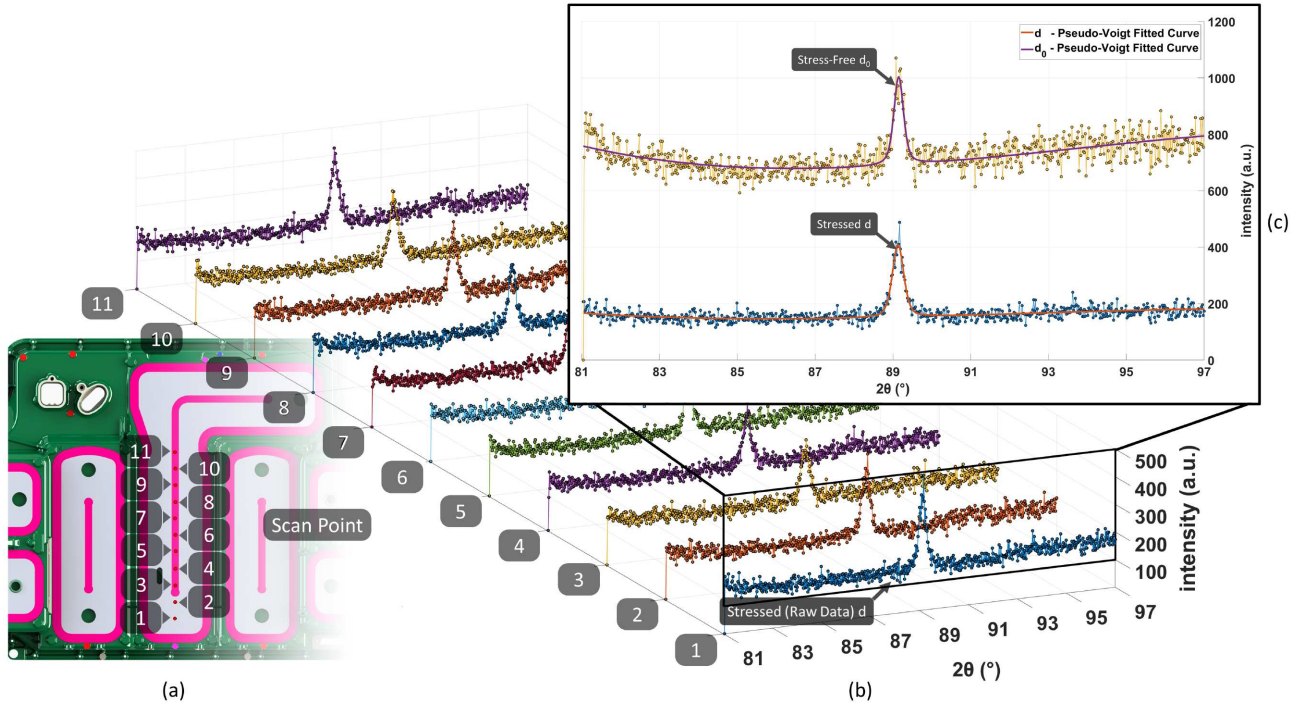


Figure 45: (a) Scan point locations; (b) Diffraction pattern of each scan point; (c) Pseudo-Voigt fitted curve for point one and the stress-free reference pattern (Sabry et al. [1])

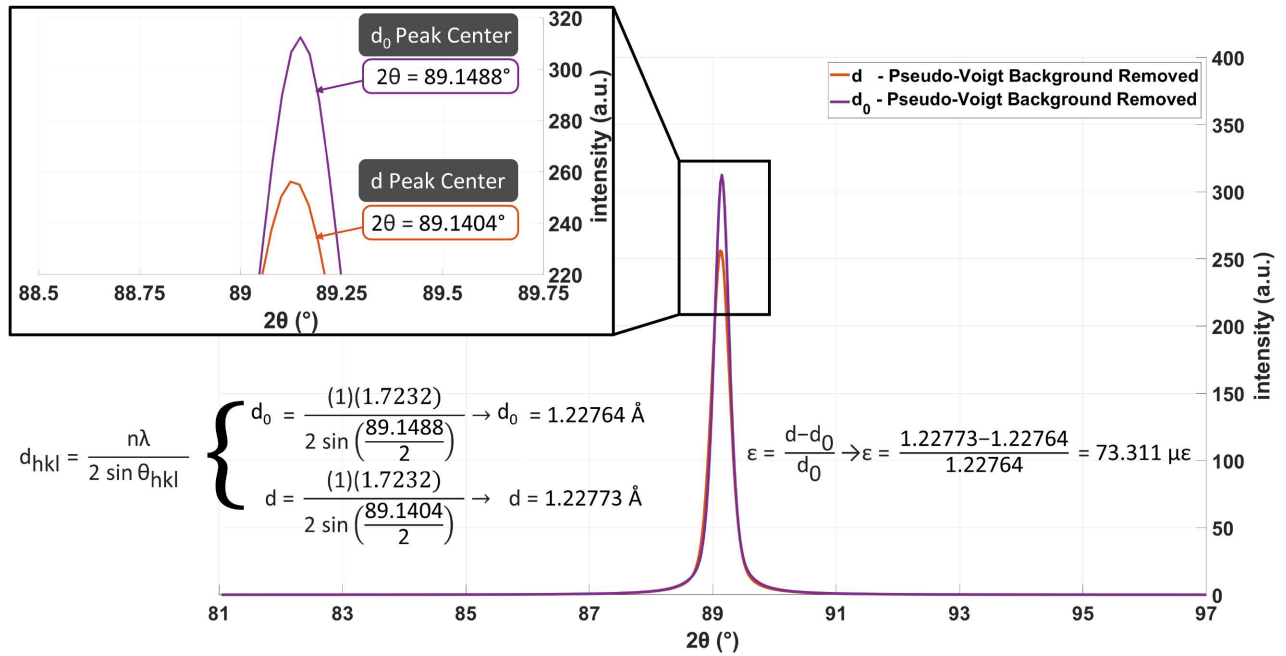


Figure 46: Calculating strain from shifted diffraction peaks with background removed (Sabry et al. [1])

The assumption that normal stresses are zero, or the plane stress condition, is often used for simplification in stress analysis. However, in complex geometries and welding processes, this assumption may not hold true. Several factors contribute to this:

- **Complex Geometry:** The use of a thin 2 mm plate welded to a much thicker component creates uneven temperature fields during welding, leading to non-zero normal stresses. Thin-wall components are particularly susceptible because they lack the strength to support residual stresses, which can result in changed d -spacing without significant normal stress. Additionally, when a plane-stress plate is welded to a thicker component, the combined thickness no longer satisfies the plane stress condition, transitioning it out of the thin-wall system and altering the stress distribution.
- **Casting:** The A365 structure in its 'as-cast' state may exhibit residual tensile stress due to contraction during solidification. This initial stress state can influence the stress profile of the welded joint, as the residual tensile stresses inherent in the casting process can compound the stresses induced during welding.
- **Material Properties:** The distinct alloying elements (e.g., copper, manganese, silicon, magnesium, zinc) and their varying percentages in different aluminum alloys (e.g., 6061 and A365) significantly influence their material properties and response to welding.
- **Microstructure:** The microstructure of the material, differing between wrought and cast aluminum, affects stress distribution in the weld. Wrought aluminum typically has a more uniform grain structure, while cast aluminum has a more heterogeneous structure with areas of different phases. These differences can lead to varying stress distributions as the weld cools and solidifies.
- **Stress Concentration:** Welding processes can lead to high-stress concentrations, especially at the transition between weld metal and the heat-affected zone (HAZ). In a lap weld configuration, the HAZ extends into the cast material below, adding to the complexity of stress distribution, unlike a simpler in-plane butt weld.

Given the complex geometry utilized in Chapter 3 (i.e., the battery tray), where a thin 2 mm plate is welded to a much thicker component below, and the similar situation in the simplified geometry discussed in Chapter 4, this dissertation does not use the in-plane stress condition. Instead, it

calculates the strain in all three directions and then determines the stress as described above. This approach is employed throughout the dissertation.

2.4.3.3 Uncertainty in Neutron Diffraction Measurements of Residual Stress

Uncertainty in residual stress determination via neutron diffraction involves various sources of error that must be carefully managed to ensure accurate results. First of all, for instrument calibration and characteristics, precise calibration is necessary for minimizing systematic uncertainties. These uncertainties can stem from misalignment, detector errors, or calibration offsets. Although it is often assumed that systematic uncertainties have been minimized, they still represent a significant potential source of error if not properly addressed (Webster et al. [127]). Another significant source of error is the properties of the sample being measured. Factors such as grain size, texture, and compositional variations can significantly impact the accuracy of strain measurements. For example, large grains can introduce variability due to the 'grain size effect,' where random uncertainties are exacerbated by local variations within the sample (Lorentzen et al. [128]). Once the sample is measured, the fitting of Bragg peaks becomes a critical source of random uncertainty. The precision of strain determination heavily relies on the accuracy of these fits. The fitting uncertainty is inherently unpredictable and must be correctly propagated through the stress calculations. This process is further complicated by additional factors, such as grain size, which can impact the fitting accuracy (Hutchings et al. [129]). Beam attenuation is another factor influencing measurement accuracy. High neutron beam attenuation occurs in regions of high stress, especially within thick components, leading to significant errors. This attenuation affects the intensity of the neutron beam, making it difficult to measure peak positions accurately and resulting in higher uncertainty (Webster et al. [127]). Errors in peak and multi-peak analysis must also be considered. Determining peak positions using Gaussian or other fitting routines is a standard part of neutron diffraction measurements but is limited by the statistical quality of the data and the symmetry of the peak. Strain-induced peak asymmetry, which can occur due to an asymmetric strain distribution within the gauge volume, complicates the interpretation of results (Lorentzen et al. [128]). Thermal effects, grain size, texture, and compositional variations all contribute to the uncertainty in residual stress measurements. These factors introduce both systematic and random uncertainties, affecting the reliability of the measurements. For instance, differential thermal expansion in multiphase materials can lead to significant discrepancies

(Hutchings et al. [129]). Uncertainties in defining the sampling volume and strain tensor determination play a key role in the overall accuracy. The precise positioning of the gauge volume relative to the scanner reference point and its shape and location within the sample are critical for accurate measurements. Misalignment or incorrect positioning can introduce substantial errors, particularly in regions with high-stress gradients (Webster et al. [127]).

Establishing a true 'zero strain' condition is a fundamental challenge in residual stress measurements. Any error in defining this baseline can propagate through the entire measurement process, leading to significant inaccuracies in the final stress determination (Hutchings et al. [129]). The lattice constants often depend on the concentration of solutes in materials, and variations in chemical composition can create a "virtual" strain. This issue can be addressed using techniques such as prompt gamma analysis (PGA) following neutron capture, which can detect concentration gradients within materials. The emitted gamma radiation provides information about the capturing isotopes and their concentrations, improving the reliability of determining the stress-free reference lattice spacing (Priesmeyer et al. [130]). Alternatively, in the case of this dissertation, utilizing physical stress-free samples across the material that take into account these variations in the concentration of solutes, such as across a friction stir weld, can be done by employing a comb sample that includes many samples across the weld, as mentioned in the section see "2.4.3.4 Stress-Free Material".

To accurately calculate the uncertainty in residual stress measurements, it is essential to consider both random and systematic components. Random uncertainties typically arise from the fitting process of Bragg peaks and the inherent statistical fluctuations in the data. Systematic uncertainties stem from instrument calibration, sample positioning, and the physical properties of the material being measured. A comprehensive uncertainty analysis must propagate these uncertainties through the stress calculations to provide a realistic estimate of the overall measurement error (Lorentzen et al. [128]). Smith et al. [13] provide quantitative values for the uncertainties of several factors discussed in this section. These include the uncertainty in wavelength calibration (approximately $\pm 0.005 \text{ \AA}$), the uncertainty of d_0 , the quality of the Gaussian or Lorentzian fit (largely affected by the attenuation of the beam by the sample materials), the uncertainty in gauge volume positioning within the sample (0.1–0.2 mm), and the beam divergence of approximately 0.6 degrees.

Additionally, the presented Pseudo-Voigt fitted curve (see Figure 45) is of high quality; however, it does not capture all experimental points with 100% precision.

In practical terms, these uncertainties necessitate careful planning and execution of neutron diffraction measurements. Techniques such as using smaller gauge volumes, optimizing beam paths to minimize attenuation, and employing precise alignment methods can help reduce these errors. Understanding the sources and magnitudes of these uncertainties allows for better interpretation of results and more informed decisions regarding material performance and safety. Ultimately, neutron diffraction is a powerful tool for measuring residual stresses in materials, but it is not without its challenges. By understanding and mitigating the various sources of error, from instrument calibration to sample properties and fitting uncertainties, it is possible to achieve more accurate and reliable measurements.

2.4.3.4 Stress-Free Material

When studying the properties across friction stir welds, the variation in solute content across the weld must be considered for the stress-free material. Barrett et al. [131] discuss in detail the influence of solute atom size and the chemical differences between solute and solvent atoms on the lattice spacing of solid solutions. In this context, the solute refers to the element being added, while the solvent is aluminum. The lattice spacing distortions are observed as a function of these chemical differences and atomic numbers, emphasizing the significance of these differences in altering the crystal lattice structure. This relationship is demonstrated through empirical evidence (see Figure 47), illustrating the impact of chemical differences and the specific behavior of aluminum-based solid solutions. It is important to note that different chemistries across friction stir welds can result in varying d_0 lattice spacings from point to point. The term "apparent strain" should be understood as representing deviations from the original state, which refers to the stress-free condition of pure aluminum due to differing stress-free points. Rather than indicating actual strain, these variations reflect changes in the lattice spacing caused by the different compositions. The term "pseudo-strain," as used by Hutchings et al. [115], may be more appropriate to describe this phenomenon interchangeably with "apparent strain." The significance of this consideration is asserted by Peel et al. [11], stating if the residual stresses had been measured using a single piece of unstrained reference material to determine d_0 , then variations of up to ± 150 microstrains may have been measured, equating to errors of $\sim 25\%$ in the aluminum 5083 friction stir weld samples

measured. To consider the variations in material for d_0 measurements, a comb-style set of stress-free samples can be extracted (see Figure 48).

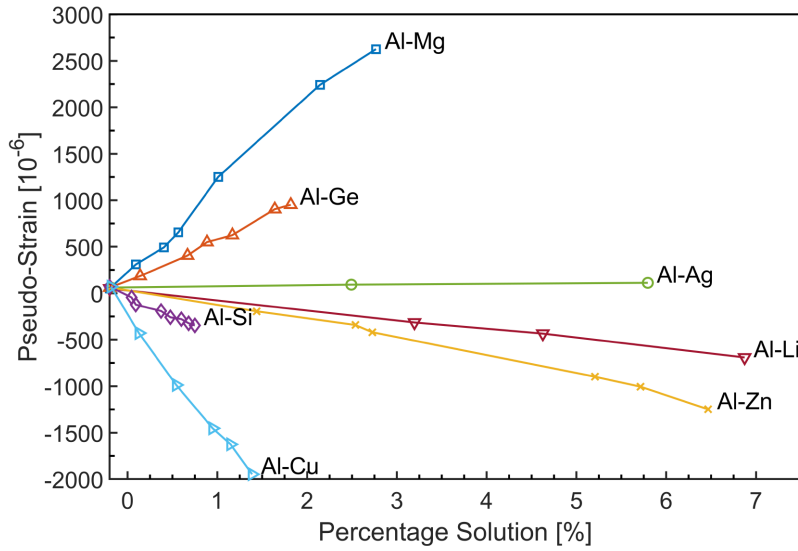


Figure 47: The variation (expressed in microstrains) of the unstrained lattice parameter with solute content for different solutes in aluminum (Hutchings et al. [132]; Barrett et al. [131]; Hutchings et al. [115])

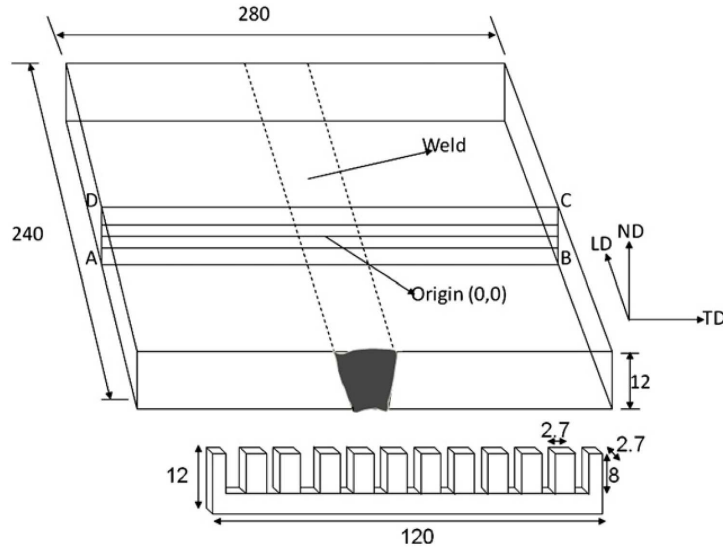


Figure 48: Schematic diagram of the welded plate measured, and the comb sample machined out from the end of the plate. LD: longitudinal direction; TD: transverse direction; ND: normal direction (Ganguly et al. [133])

The stress-free reference is obtained by cutting the material into small sections, often referred to as matchsticks, to alleviate stress from the surrounding material. The matchstick geometry, with a cross-sectional side in the range of 2-4 mm and a length of 8 mm or longer, ensures the full release

of stresses in the X-Y directions of the cross-section. This geometry can be square or rectangular. In some experiments, particularly where the material chemistry or microstructure changes rapidly over short distances, it is convenient to arrange these stress-free reference samples into a comb-shaped gallery of matchsticks, which accounts for compositional and microstructural differences across the weld caused by the thermomechanical cycle of welding. Local fluctuations cause changes in the stress-free lattice; each tooth of the comb considers these fluctuations while standing insufficient in dimension to hold a stress field in the scanning direction.

Type I and Type II stresses, also known as macrostresses and microstresses, respectively, are released when a material is cut into small shapes, such as the teeth on a comb sample less than 3 mm in size (see Figure 48). These stresses affect peak positions in diffraction measurements by altering the atomic planes' spacing, which causes shifts in the diffraction peak positions according to Bragg's Law. Upon cutting, the release of Type I and II stresses allows for the measurement of "stress-free" material. However, Type III stresses, in contrast, remain after cutting and cause broadening of the diffraction peaks rather than shifting them. These microstresses exist within individual grains and vary from point to point, resulting in a range of different lattice plane spacings within each grain. This variation leads to a range of different diffraction angles, observed as a broadening of the diffraction peak.

Both Type II and Type III stress contribute to peak broadening in diffraction patterns. For example, the full width at half maximum (FWHM) signal, which measures the width of the diffraction peak at half its maximum intensity (see the diffraction peak in Figure 45 and Figure 46), is used to analyze this broadening. However, it is nearly impossible to differentiate between Type II and Type III stresses based on the FWHM signal alone. When studying Type II stresses, such as fatigue damage accumulation, it is important to acknowledge that a small component of Type III stress is also present in the signal. The significance of Type I, II, and III stresses in diffraction patterns varies depending on material properties, manufacturing processes, and specific experimental conditions. Type I and II stresses, being macroscopic and microscopic in nature, generally have a more significant effect on peak positions because they cause uniform strain over some length scale in the material (see Figure 22), changing the atomic planes' spacing.

In the context of friction stir welded aluminum material, grain size and orientation significantly influence the initial stress state by affecting Type II stresses. As previously described, while solute content directly affects stress-free measurements, grain orientation and size impact the initial stress state that is in reference to these stress-free measurements, ultimately affecting the final residual stress results, which are primarily supposed to be Type I stresses. The influence of other stress types on residual stress is considered by the planes selected for diffraction as both Type II and Type III stresses are mitigated by selecting diffraction planes with low sensitivity to intergranular stresses and strains, such as the $\{311\}$ plane for aluminum (ISO/TS 21432:2005 [123]). Although Type III stresses are primarily considered for broadening effects, the chosen diffraction plane with low sensitivity to intergranular stresses and strains still means that grain sizes and orientations specifically affecting Type II stresses can have an impact, as the sensitivity is low but not zero.

Stress-free measurements are often obtained from small cubes machined from the weld instead of the comb-style sample shown in Figure 48. Ganguly et al. [133] compared both measurement methods and found that the teeth size in the comb sample is small enough to alleviate the macro-stress field except in the direction of maximum dimension. Paradowska et al. [134] conducted a comparative study between the comb and cube methods and measured nearly identical d-spacing values for the stress-free references. While a comb sample, as previously used by Sediako et al. [16] and Liu et al. [135], serves as a valid baseline for residual stress calculations, it is important to note that a comb sample is a combination of many matchsticks joined on one side. Stress-free conditions exist only in the two directions of the single tooth (matchstick) cross-section distanced from the joint end. In contrast, a cube has minimum size on all three dimensions, satisfying stress-free conditions in all directions. Therefore, while both methods are effective, the choice between a comb and a cube can depend on the specific requirements of the stress measurement and the nature of the material being analyzed.

2.4.4 Section Summary

Methods for measuring residual stress in FSW are outlined, emphasizing why specific non-destructive and destructive techniques are not suitable for this study's objectives. Non-destructive techniques like X-ray diffraction lack the necessary penetration depth and introduce uncertainties when applied to aluminum friction stir welded components. Destructive methods such as hole-drilling, while cost-effective and quick, are unsuitable due to their localized measurement

capabilities and the cumulative damage they cause, affecting subsequent measurements. This makes both methods inadequate for analyzing large areas such as the battery tray, where understanding Type 1 component-wide residual stresses is important. Neutron diffraction, in contrast, offers deeper penetration, and with specific beamlines such as HIDRA and Kowari, large components can be handled for residual stress measurement. This method allows for comprehensive stress mapping, making it ideal for the detailed analysis required in this study. Understanding Bragg's law and Laue's equations is essential for interpreting neutron diffraction results, as they explain how the diffraction angles and wavelengths relate to the lattice spacing in the material. The focus on the $\{311\}$ plane, which is less affected by intergranular strains, is crucial for accurately assessing Type 1 residual stress and directly supports the study's objectives. Using a 90-degree angle for a given wavelength simplifies the gauge geometry and data processing. Additionally, using stress-free reference materials is necessary due to the variation in solute content across friction stir welds. In aluminum alloys, significant changes in the unstrained lattice parameter can occur due to solute variations, which must be considered to avoid significantly over- or underestimating residual stress. This ensures accurate stress measurements across the weld. Finally, the decision to avoid the plane stress assumption and calculate strain and stress in all three directions provides a more accurate representation of residual stresses in complex geometries, utilizing the lap weld configuration between dissimilar aluminum alloys. This approach is essential for the comprehensive analysis presented in Chapters 3 and 4, ensuring the reliability and relevance of the findings.

2.5 Measuring Texture in Friction-Stir-Welding

This section provides a brief overview of the fundamentals of the scanning electron microscope (SEM) and the related electron backscatter diffraction (EBSD) technique. All texture analyses in this dissertation utilize EBSD, making it crucial for many of the conclusions drawn, which is why it is highlighted here. EBSD offers exceptional resolution for detecting minute differences in texture. By slightly sacrificing resolution, new EBSD equipment can even analyze extremely large areas of material, including full welds, in the friction stir welding process. This capability, though rarely documented in the literature, allows for efficient comparisons between different welding parameters and their associated textures, as entire regions can be compared across welds.

2.5.1 Scanning electron microscopy

Scanning electron microscopy (SEM) is an imaging method that delivers much higher resolution capabilities than optical microscopes. The resolution depends on the wavelength of the imaging beam; visible light, used in optical microscopes, spans approximately 380 to 760 nm (Ul-Hamid et al. [136]). Shorter wavelengths allow for finer detail because they can resolve structures comparable in size to their wavelength, which means they can interact with and reflect off smaller features, providing a clearer image of fine details. Electrons accelerated to 15-30 keV, as employed in this study, have a wavelength of about 0.01 nm. This shorter wavelength is achieved because accelerated electrons have higher energies, and according to the de Broglie principle, the momentum (which increases with energy) inversely affects the wavelength, resulting in a shorter wavelength.

Using electrons for imaging necessitates a vacuum environment to prevent scattering by air molecules, which would degrade the image quality or potentially prevent image formation. SEM requires careful sample preparation to address issues such as the charging of the sample. Non-conductive samples or non-conductive holders can accumulate charge under the electron beam, distorting the image. This can be mitigated by coating the sample with a thin conductive layer or using copper tape. Additionally, the sample must be polished to a fine degree, and oxide layers on materials like aluminum can alter image quality by changing the surface's electrical properties. Once properly prepared, SEM allows for highly detailed and magnified observations of sample microstructures. Figure 49b illustrates the layout of a scanning electron microscope and the various signals generated from the interactions of accelerated electrons within different regions of the sample. Electrons are emitted from a field emission gun due to an intense external electric field. A high voltage then accelerates these electrons, and an electromagnetic lens aligns the resulting beam. After passing through an adjustable aperture, the beam is periodically deflected by a set of scanning coils, resulting in a line scan from the top to the bottom of the screen, sweeping across the selected area of the sample surface. The deflected beam is then focused by the objective lens and interacts with the sample. The interaction of primary electrons with the sample results in various types of radiation originating from different depths beneath the sample surface (see Figure 49a). One such useful radiation is characteristic X-rays, which are often used to analyze the local chemical composition of the sample, known as energy-dispersive X-ray spectroscopy (EDS).

However, the key signals for this work are secondary electrons and, most importantly, backscattered electrons for texture analyses. Secondary electrons are expelled from the electron shells of sample atoms by primary electrons. Due to their low energy, they originate from a very shallow layer of the sample, typically around 1 nm in metals. The secondary electron signal is sensitive to surface orientation, providing topographical contrast for surface observations, such as distinguishing areas of high silicon content (A356) from those with low silicon content (6061). Backscattered electrons, on the other hand, are almost entirely elastically scattered by atomic nuclei within the sample at acute angles that enable them to exit the sample surface. Their interaction volume in metals is typically in the range of hundreds of nanometers due to their higher energy compared to secondary electrons. The backscattered electron signal provides composition contrast based on variations in scattering amplitude with the atomic number of the nuclei (Ul-Hamid et al. [136]). This is important for electron backscatter diffraction (EBSD), which takes advantage of specific crystallographic patterns to analyze the texture of materials.

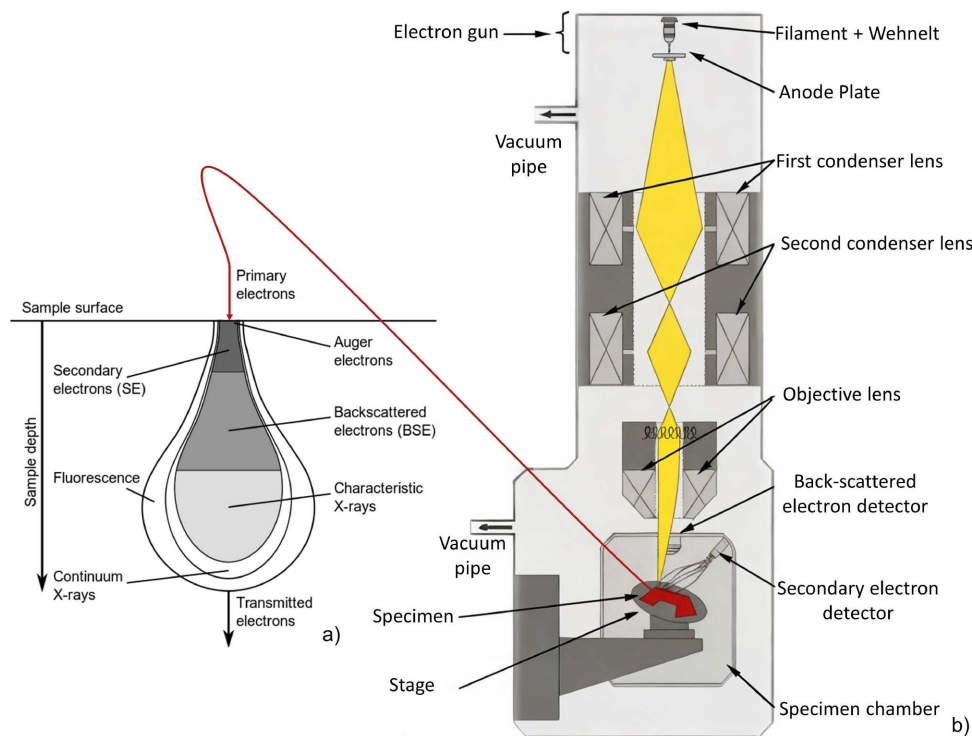


Figure 49: a) Schematic representation of the interaction volumes from which specific signals in SEM originate; (b) Diagram of the scanning electron microscope (Sutton et al. [137])

2.5.2 Electron Backscatter Diffraction

Electron backscatter diffraction (EBSD) is a powerful microstructural analysis technique used to investigate the structure and texture of materials. In this method, an electron beam scans the sample surface in a grid-like pattern, and at each point, the diffraction of electrons by the crystal lattice is measured. This allows for the determination of local crystallographic orientations. EBSD provides both qualitative and quantitative insights into various characteristics such as grain boundaries, grain size distribution, texture, and misorientation between individual grains (Ul-Hamid et al. [136]). Kikuchi lines, utilized in EBSD, are formed through a series of diffraction events involving Bragg diffraction conditions for electrons, which create Kossel Cones.

1. Initial Scattering: The primary electrons from the electron beam interact with the specimen and undergo incoherent scattering, meaning they scatter randomly due to thermal vibrations or inelastic interactions (see Figure 50a).
2. Secondary Scattering (Bragg Diffraction): Some of these incoherently scattered electrons (backscattered electrons) then undergo a secondary scattering event, which is a coherent (elastic) Bragg diffraction. This secondary Bragg diffraction aligns the scattered electrons with the crystal planes, forming distinct patterns (see Figure 50b).
3. Formation of Kikuchi Lines: The secondary scattering event (Bragg diffraction) of these backscattered electrons leads to the formation of intensity excesses and deficits, which, when organized in three dimensions, create Kossel cones. The intersections of these cones with the viewing screen in an EBSD setup form the nearly straight Kikuchi lines (see Figure 50c).

The formation of Kikuchi lines begins with incoherent scattering that creates a forward-peaked diffuse background. This forward-peaked diffuse background refers to a spread of scattered electrons predominantly in the forward direction, leading to a less defined, somewhat fuzzy appearance behind the Kikuchi lines. This diffuse background is why Kikuchi lines appear on a gray or less distinct background. (see Figure 50a). Some of these incoherently scattered electrons undergo a secondary Bragg diffraction, leading to regions of intensity excesses and deficits. These regions of intensity excesses and deficits result in strong dark lines and lighter gray lines in the Kikuchi pattern. This occurs because the secondary Bragg diffraction creates areas where electrons

constructively or destructively interfere, producing the observed pattern. Essentially, a series of Kossel cones form the full Kikuchi pattern. (see Figure 50b). These regions, when organized in three dimensions, form Kossel cones, which intersect with the viewing screen to produce Kikuchi lines (see Figure 50c). This interplay between incoherent scattering and secondary Bragg diffraction results in the Kikuchi lines observed in electron diffraction patterns, providing information on the crystallographic planes and their orientation (see Figure 51).

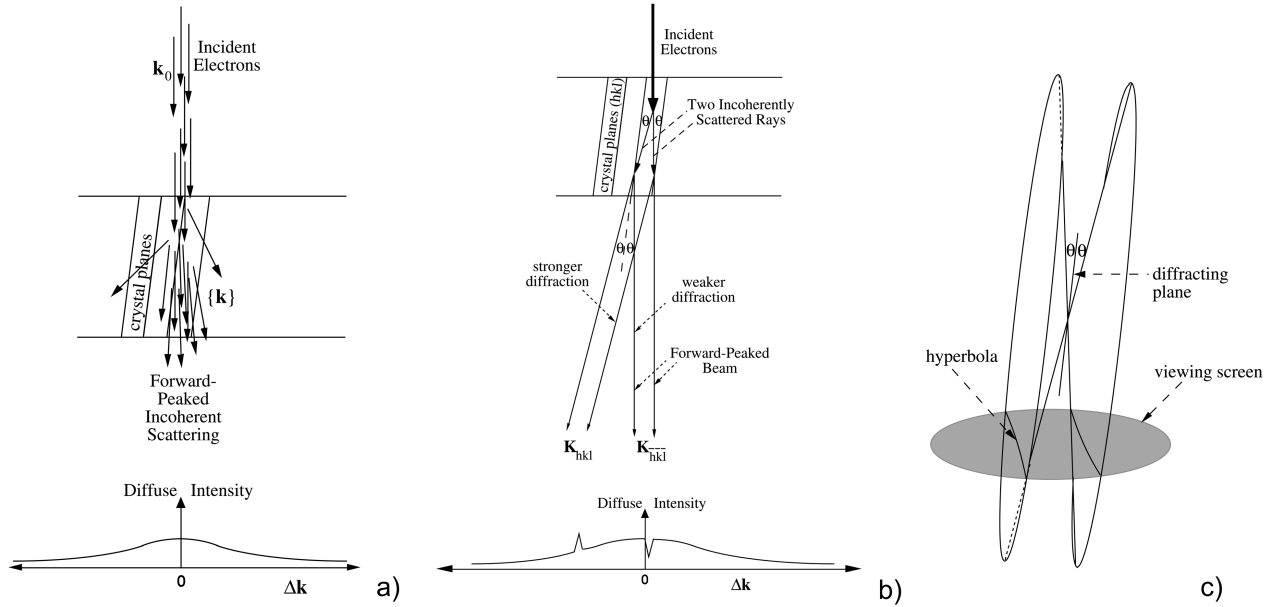


Figure 50: (a) Top: Electron wavevectors before and after incoherent scattering. Bottom: Diffuse intensity from forward-peaked incoherent scattering; (b) Top: Electron paths with incoherent scattering and Bragg diffraction. Bottom: Intensity pattern showing sharp modulations from Bragg diffraction; (c) Intersection of Kossel cones with the viewing screen (Fultz et al. [138])

To determine the crystal orientation from the diffraction patterns, specialized software analyzes the positions of the Kikuchi lines using the Hough transform (Duda et al. [139]). The software calculates the mutual angles of the lines and compares them to a database of interplanar angles for the specific crystal structure being studied. The best matching orientation is then assigned to that point on the sample surface (Wright et al. [140]). The accuracy of this match is quantified by a metric known as the confidence index. By systematically and automatically analyzing the diffraction patterns across all points on the sample, the software generates an EBSD map. Figure 51c depicts this EBSD map and how it relates to the spherical Kikuchi maps, which are used to represent the diffraction patterns in three dimensions.

Different crystal orientations (such as (100), (110), and (111) planes) are shown, indicating how the Kikuchi patterns change with varying orientations. The diagrams at the bottom show how these patterns relate to the orientation of the crystal planes within the sample.

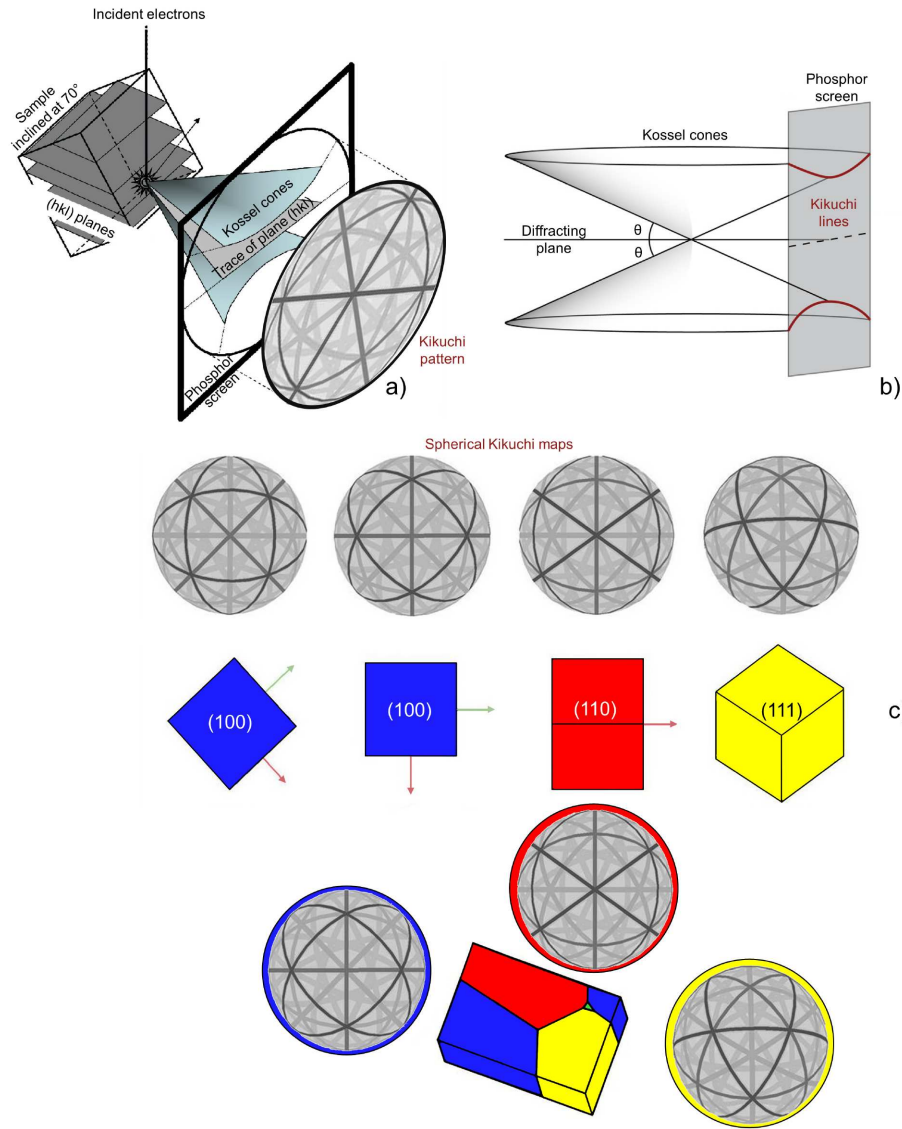


Figure 51: (a) Sample at 70° produces Kikuchi patterns; (b) Kossel cones intersect the screen, forming Kikuchi lines; (c) Kikuchi diffraction patterns for different crystal orientations (Adapted from Bouscaud et al. [141]; Herron et al. [142]; Oxford Instruments; EDAX)

Lastly, as shown in Figure 51a, the sample is inclined at a 70-degree angle. This specific inclination serves several crucial purposes. Firstly, it enhances the backscattering yield by optimizing the interaction volume between the electron beam and the sample surface, resulting in a higher yield of backscattered electrons, which improves the quality and intensity of the Kikuchi patterns,

making them easier to detect and analyze. Secondly, the tilt aligns the crystal planes more favorably with the incident electron beam and the EBSD detector, enhancing the conditions for Bragg diffraction and leading to clearer and more defined Kikuchi lines. Additionally, the 70-degree tilt reduces background noise and enhances the contrast of the Kikuchi lines, thus improving the signal-to-noise ratio crucial for accurately determining crystallographic orientations. Finally, this inclination ensures efficient detector positioning, directing the scattered electrons toward the detector more effectively, thereby maximizing the detection of the backscattered electrons that form the Kikuchi patterns (Schwartz et al. [40]). These combined factors significantly improve the quality and reliability of EBSD measurements, allowing efficient acquisition of grain and texture data within sufficiently prepared samples.

2.5.3 Section Summary

EBSD is highly effective for analyzing the texture in Al alloy friction stir welds due to its detailed microstructural information capabilities, including grain size, orientation, and boundary characteristics. This detailed analysis is crucial for understanding the properties of the weld and how they influence yield strength, as discussed in the section “2.2.5 Mechanical Properties of DXZ, TMAZ, and HAZ”. EBSD’s accuracy stems from its reliance on the fundamental principles of Bragg diffraction. It excels in capturing the complex and three-dimensional texture variations induced by the Friction Stir Welding process. Unlike other texture analysis methods that require larger sample volumes, EBSD can provide highly detailed maps down to very small regions, limited only by the time invested in the analysis. FSW causes dynamic recrystallization, significantly impacting the grain structure and, consequently, the material properties. EBSD efficiently detects these microstructural changes, offering valuable insights into the effects of the welding process (Liu et al. [57]; Liu et al. [79]; Kar et al. [143]; Patel et al. [144]; Nourani et al. [145]; Moradi et al. [146]). This makes EBSD an indispensable tool in this dissertation for understanding the evolution of microstructure and properties in Aluminum Alloy Friction Stir Welds.

2.6 Background Conclusion

This chapter provides the fundamental background necessary for this dissertation. Friction Stir Welding (FSW) is a progressive welding process, especially suitable for materials traditionally

deemed non-fusion weldable, such as aluminum alloys. During FSW, the combination of thermal cycles and mechanical stirring generates a unique microstructure that encompasses a dynamically recrystallized zone (DXZ), a thermomechanically affected zone (TMAZ), and a heat-affected zone (HAZ). These zones exhibit variations in microstructure, texture, and precipitation patterns, resulting in significant alterations in mechanical properties along the weld line.

Similar to other welding processes, FSW induces thermal residual stresses and potential distortions in components. Residual stresses, which are internal stresses remaining in the absence of external forces, typically display an M-shaped distribution along the weld line in aluminum alloys. Peak tensile stresses, often reaching 60-70% of the base material's yield strength at room temperature, are commonly found around the TMAZ and HAZ. While transverse and normal residual stresses are usually minor, they can be relevant in specific scenarios. Distortions arising from these stresses can manifest as concave or convex bending, angular deformation, and buckling, especially in thin plates joined via FSW.

Mitigation of residual stresses can be achieved through the optimization of welding parameters. Additionally, implementing stress engineering techniques—whether thermal or mechanical—during or post-welding can further alleviate tensile stresses or even introduce compressive residual stresses along the weld line. Thermal methods, though effective, are often intricate and challenging to control, whereas mechanical techniques like coining and burnishing are easier to apply but may increase production costs and time. Notably, limited experimental research has been conducted on multi-welded components.

The effectiveness of stress engineering methods can be assessed through non-destructive testing techniques like neutron diffraction. This approach leverages the crystal structure of the material as an inner-material strain gauge, with diffraction patterns shifting according to Bragg's law in response to stress-induced changes in lattice spacing. By analyzing these diffraction patterns, one can derive critical insights into the stress state of the material. However, this requires accurate measurement of the unstrained unit cell parameter, which varies with alloying element distribution across the weld, for precise stress assessment in aluminum alloy welds.

A review of current industrial applications of FSW, particularly in battery tray production, has outlined several objectives for this dissertation.

Objectives for Chapter 3:

1. Investigate Distortion, Residual Stress, and Texture:
 - Assess the distortion, residual stress, and texture of the as-welded battery tray.
 - Compare these parameters between the as-welded and straightened battery trays.
2. Identify Clues to Residual Stress Susceptibility:
 - Determine if the proximity of welds influences residual stresses in the as-welded battery tray.
3. Analyze Texture:
 - Characterize the texture of the as-welded battery tray.
 - Explore the relationship between texture and residual stress.
4. Examine Texture Variation:
 - Investigate if there are significant texture variations between different welds on the battery tray.
 - Evaluate how these variations might influence the mechanical properties of the tray.
5. Compare Distortion Among Different Straightening Methods:
 - Compare the distortion in as-welded, burnished, straightened, and coined straightened battery trays.
6. Assess Residual Stress Redistribution:
 - Analyze how residual stress redistributes after each straightening operation.
 - Determine if the residual stresses become more favorable (i.e., less tensile and more compressive) after straightening.

Objectives for Chapter 4:

1. Analyze the Effect of Varying Speed Parameters:
 - Investigate how varying speed parameters, given a constant speed ratio, affect the texture and residual stress in FSW components.
2. Identify Optimal Welding Speed:
 - Determine if there is a specific welding speed that results in the desired texture and reduced residual stress in FSW components.
3. Examine Weld Sequence Effects:
 - Analyze how the sequence of welds in multi-welded components influences distortion and residual stress.

4. Identify Effective Welding Sequences:

- Identify welding sequences that result in reduced distortion and residual stress in multi-welded FSW components.

The following chapters will present the experimental techniques employed and the results obtained from the aforementioned objectives.

Chapter 3: Analysis of Stress, Texture, and Distortion in Friction Stir Welded Battery Tray

This chapter encompasses the research carried out on the EV battery tray joint via the friction stir welding process. The experiments executed in this chapter are separated into the following sections, which consider the initial examination of the as-welded battery tray and an in-depth discussion of the subsequent findings. The next segment explores the impact of the straightening processes on the tray and the resultant outcomes.

3.1 Characterization of FSW Microstructure and Residual Stress in the As-Welded Non-Straightened Battery Tray

This section investigated the residual stresses in the lap 6061 to A365 friction stir weld joint. Neutron diffraction is utilized to measure the d -spacing of various locations on a battery tray and stress-free reference samples to calculate the residual stresses. Using electron backscatter diffraction and optical microscopy analyses, the study also associates certain measured residual stress to microstructure variations. The results showed that the FSW operation affects the chemical composition, creating varying elastic anisotropy across the weldment, which creates unique d -spacing profiles even in a stress-free environment. Cooling rate, rather than peak temperature during welding, is considered to have a greater influence on the maximum resulting tensile residual stress.

3.2 Microstructural Changes in Friction Stir Welds of the Battery Tray

This section investigates the effects of plastic deformation on the mechanical properties of the lap 6061 to A365 friction stir weld joints on the battery tray, focusing on the influence of tool rotation, traverse speed, and plunging force. The results characterize two distinct weld groups with different welding parameters by employing electron backscatter diffraction, microhardness, and inductively coupled plasma emission analyses. The grain structure analysis reveals distinct zones with varying microhardness. The study finds that increased plastic deformation, driven by higher rotational speed, traverse speed, and plunging force, leads to variations in mechanical properties between the weld groups rather than the distribution and generation of precipitates

3.3 Analyzing Distortion and Stress Effects in Response to Straightening the Battery Tray

This section analyzes the effect of residual stress on the structural integrity of the battery tray after straightening. Neutron diffraction analysis was used to characterize the residual stresses generated during the welding process, and after burnishing and coining operations were used to mitigate the distortion. The results show that the FSW operation generates residual stresses exceeding the material's yield strength, causing the tray to deform from its pre-weld geometry. However, burnishing and coining operations restore the tray's geometry to within acceptable limits, although the latter increases the number of locations where the residual stress exceeds the material's yield strength.

3.1 Characterization of FSW Microstructure and Residual Stress in the As-Welded Non-Straightened Battery Tray

Joining cast aluminum components with wrought metals is becoming an efficient design strategy frequently utilized in the automotive industry. For example, this strategy has recently been employed to develop complex and large body structures that require lightweight solutions further by enabling different manufacturing techniques such as casting, extrusion, and forming in a single joint component. One such casting technique is high-pressure die-casting (HPDC), which utilizes pressure-injected molten aluminum into a steel mold to form thin-walled components. After the system's initial cost, the HPDC technique is fast and inexpensive in producing components as it delivers near-net structures with minimal post-cast machining required (Kridli et al. [147]). However, during the HPDC operation, turbulence is generated in the liquid metal due to the high velocity required to produce thin-walled castings. Unfortunately, turbulent conditions cause porosity and gas entrapment. As a result, the entrapped gas of air, hydrogen, and vaporized die lubricants render fusion welding an impractical joining method unless expensive vacuum HPDC equipment is employed to minimize porosity (Wang et al. [148]). Furthermore, fusion welding struggles with dissimilar-material welding as fusion requires the materials to melt before joining. Because dissimilar materials have different melting temperatures, the alloying elements required for strength and other properties burn in the material with lower melting temperatures before the second material reaches the required temperature for fusion, creating a compromised weld (Soysal et al. [149]). Additionally, there are several other challenges related to dissimilar welding due to differences in the joined materials' mechanical, physical, chemical, and metallurgical properties (Khan et al. [150]; Çam et al. [151]). While there are still obstacles to surmount, the FSW technique invented by Thomas et al. [152] has successfully been demonstrated to join dissimilar materials in

a solid state, i.e., below-melting temperature. The solid-state nature of FSW avoids many problems, one of which is the joining of materials with different melting temperatures. The joining of dissimilar materials with FSW is achieved by severe plastic deformation of both materials, utilizing heat generated by a rotating tool plunged into the workpiece and advanced along the weld joint, mechanically mixing and forging plasticized dissimilar material together. The severe plastic deformation leads to dynamic grain recrystallization, enabling the flow of plasticized material occurring in the solid-state weld (Zhao et al. [153]). Consequently, the recrystallization results in the formation of equiaxed and submicron grains in the weld zone after complete solidification. Therefore, FSW is a highly effective method for joining dissimilar materials, as the operation produces a weld joint with high mechanical strength due to the associated reduction of grains and the operation's solid-state nature (Mohammadi et al. [154]).

Although the FSW method is effective in joining dissimilar materials, the heat generated during the operation and subsequent cooling of the body leads to the evolution of residual stress in that body. Typically, for similar-alloy aluminum FSWs, the maximum tensile residual stresses are on either side of the weld line, with lower residual stresses in between, forming an "M" shaped stress profile distribution. However, as described by the literature, this stress profile is heavily influenced by several factors, including the heat generation of the tool geometry, tool rotational speed, tool traverse speed, and plunging force (de Giorgi et al. [155]; He et al. [12]; Peel et al. [11]; Nie et al. [156]). Additionally, Giorgi et al. [155] show an example specific to tool geometry and its effect on the residual stress profile on a 1.5 mm thick 6082-T6 aluminum sheet. It was observed that when the tool geometry was modified, the tool's heat generation in the material was influenced significantly. As a result, it was concluded that tool geometries with a larger contact area between the tool shoulder and material surface produced higher peak temperatures, decreasing the maximum tensile residual stress. In comparison, Aval et al. [157] examined the effect of rotational speed and traverse speed on the resulting residual stress in 8 mm thick dissimilar welded 6082-T6 and 7075-T6 aluminum plates. They found that as the rotational speed increased and the traverse speed decreased, both actions increased the material's heat generation, raised peak temperatures, and decreased the maximum tensile residual stress. The result of additional heat input and its effects on residual stress for Aval et al. [157] contradicts the result of Giorgi et al. [155]. The contradicting results might be explained by Richards et al. [6], who described that the stress profile from FSW is due to the misfit of plastic strains that are introduced through steep temperature

gradients as heat is generated from the FSW operation and distributed into the component during the advancement of the tool during welding. Therefore, combining the conclusions presented in the available literature suggests that peak temperatures alone do not determine the maximum tensile residual stress. Instead, the gradient in temperature or, more specifically, the material cooling rate and its uniformity has a more pronounced influence on the maximum tensile residual stress. The above statement is supported by the results from Campanelli et al. [158], who analyzed the effects of preheating the weld zone with a high-powered laser; in this research, it was found that by preheating the work material, the peak temperatures increased, and the maximum tensile stress decreased, not because of the increased peak temperature but due to a control of the cooling rate, which reduced the steep gradients in temperature that the material experienced during welding. Additionally, there might be a significant difference in the cooling rate between FSW operations conducted on similar welded material and dissimilar welded material. Where FSW is conducted on dissimilar material, one must also consider the weld joint's abrupt property changes due to the weldment's heterogeneity. If, for example, thermal expansion coefficients of dissimilar metals vary significantly, it will give rise to the internal stress evolution in the weldment during every temperature change (Liu et al. [159]). Whether the FSW operation is conducted on dissimilar or similar material, a significant amount of residual stress develops from the inevitable temperature gradients generated during the operation. Depending on the amount and type of residual stress, the friction-stir-welded component's structural performance, reliability, and service life may be reduced considerably.

A few mechanisms that heavily depend on the type and amount of residual stress in a component are fatigue crack propagation and distortion. For example, cracking caused by fatigue will self-arrest in compressive residual stress fields and propagate in tensile residual stress fields (Woodtli et al. [76]). Additionally, if the amount of residual stress surpasses the yield stress, the component will distort in response not only due to tensile stress surpassing yield stress but by the entire magnitude of the difference between the peak tension and peak compression. The distortion mechanism is particularly pronounced in thin-walled HPDC structures (i.e., reduced cross-sectional areas) (McCune et al. [160]). Therefore, a comprehensive understanding of the residual stress is required to maintain the longevity of consistent and high-quality friction stir welds on HPDC components required in the automotive industry.

This chapter focuses on joining a thin-walled HPDC component (A365) to several plates (6061) utilizing FSW. The joined component completes the manufacturing stage of an electric vehicle battery tray; however, the welding operation distorts the component due to the generation of residual stress. To investigate the residual stress that leads to the distortion of the component, this study utilizes neutron diffraction for its deep, non-destructive material penetration, allowing for the residual stress measurement in an unaltered battery tray. Furthermore, the residual stress profile is linked to microstructure marks left by the FSW operation, utilizing an optical microscope and EBSD. This evaluation enables a better understanding and application of many parameters, such as tool geometry, weld direction, and initial temperature conditions, to minimize high-stress evolution in FSW components.

3.1.1.1 Materials and Weld Process

The battery tray encompasses several 6061-T6 aluminum plates with a 2 mm thickness friction stir welded onto an A365 high-pressure die-casted aluminum component with varying thicknesses (5 - 10 mm). The plate and casting are joined in a lap weld configuration (see Figure 52b). The tool is plunged through the entirety of the 2 mm plate and slightly into the A365 casting to provide a solid bond between the two materials. The difference in chemical composition and processing leads to different precipitates or particles that locally change the alloys' stiffness and other properties (see Table 1). A significant difference between A365 (cast) and 6061 (wrought) alloys is in the silicon composition, as one benefit of additional silicon is the increase in cast fluidity of the A365 alloy when the molten material is forced throughout the die in the HPDC operation (Wang et al. [24]). However, with the increase in silicon, there is a corresponding decrease in the thermal expansion coefficient, which could have significant implications as the materials are heated and cooled through welding (Hidnert et al. [28]).

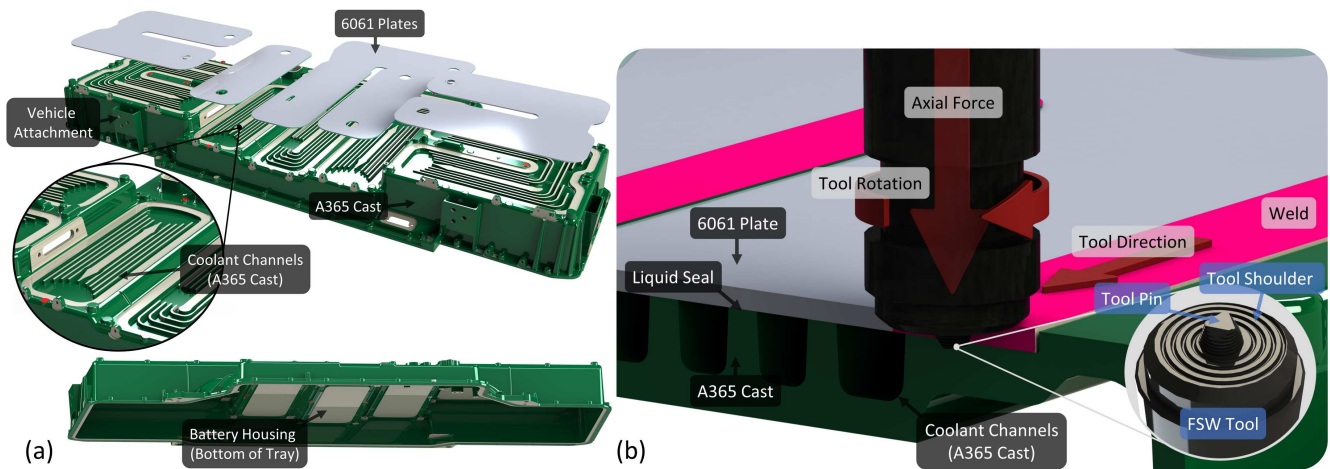


Figure 52: (a) Battery tray coolant channels, cast structure, and plate position; (b) Cross-section of battery tray coolant channel with weld path in magenta

The HPDC operation and subsequent FSW used to manufacture the battery tray was conducted at the Nemak Alabama plant, United States. The manufacturing begins by casting the battery tray's main structure and coolant channels with the A365 aluminum alloy. Because the HPDC process is limited in creating internal geometries, the FSW operation is required to seal all exposed cooling channels with 6061 plates (see Figure 52a). Unfortunately, the FSW operation leaves behind a “keyhole” defect where the tool is retracted at the end of the operation. This portion of the weld is the weakest, and several methods exist to repair this defect (Mehta et al. [161]). However, the keyhole defect falls outside the scope of the section. Nonetheless, the FSW operation in this study utilizes a tool with a 12 mm diameter shoulder that is filleted to a 14 mm diameter with a triangular M12 threaded 2.2 mm long pin (see Figure 52b). The operation is divided between peripheral and non-peripheral welds (see Figure 53), with different traverse speeds, rotational speeds, and plunging forces, as described in Table 2.

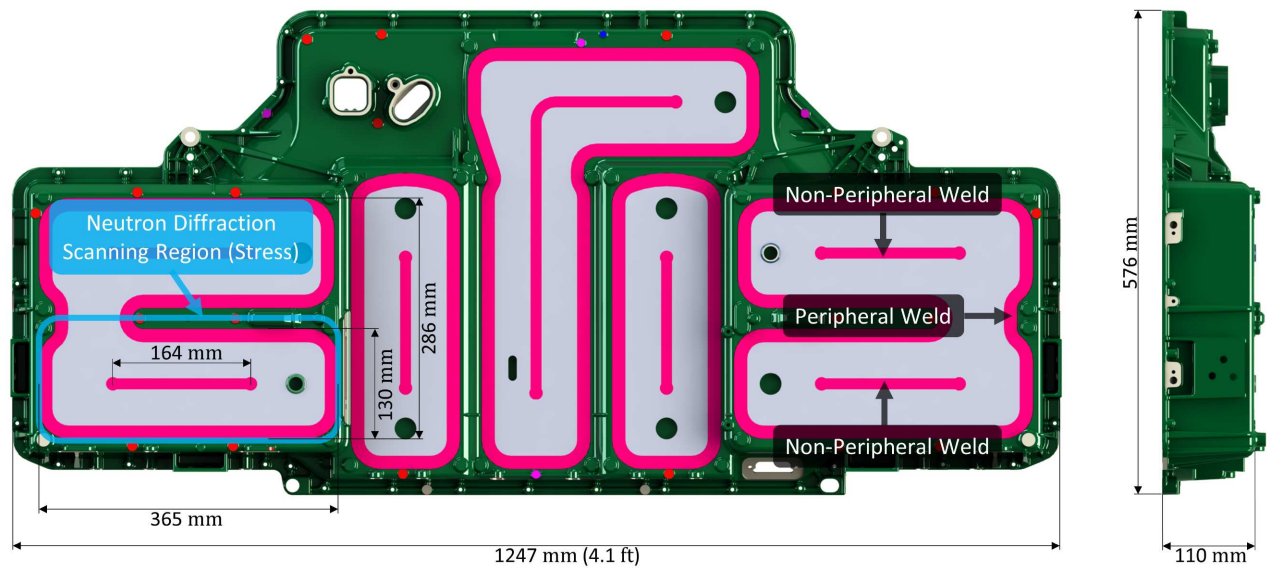


Figure 53: Peripheral and non-peripheral weld placement on the battery tray, welds in magenta, and scanning region outlined in blue

Table 2: FSW processing conditions (battery tray)

Weld Type	Plunge Depth (mm)	Dwell Time (s)	Traverse Speed (mm/min)	Rotation Speed (RPM)	Plunge Force (kN)	Plunge Feed (mm/min)	Withdraw Feed (mm/min)
Peripheral	2.2	1.2	1300	2000	5.7	120	250
Non-peripheral	2.2	0.2	2300	3600	10	120	250

Please note that a constant speed ratio (rotational speed/traverse speed) of nearly 1.5 is held between the two processing conditions, with the plunging force requiring a significant increase to maintain a defect-free weld at the higher speeds of the non-peripheral (center) weld.

3.1.1.2 Microstructure Sample Preparation

The microstructural analysis is completed on a metallurgical sample extracted from the cross-section of a weld line that combines the 6061-wrought and A365-cast material (see Figure 54). First, the sample was mounted and polished following the principles outlined in the ASTM E3-11 standard. Following polishing, the specimen was electro-etched using Barker's etchant (5 ml of fluoroboric acid diluted with 200 ml of distilled water) for 15 seconds with an applied voltage of 30 V through a stainless-steel anode and cathode. Following etching, the sample was rinsed in distilled water and air-dried. Next, a microstructure analysis was completed with an optical microscope and scanning electron microscope (SEM) utilizing acquisition and processing software for composition analysis.

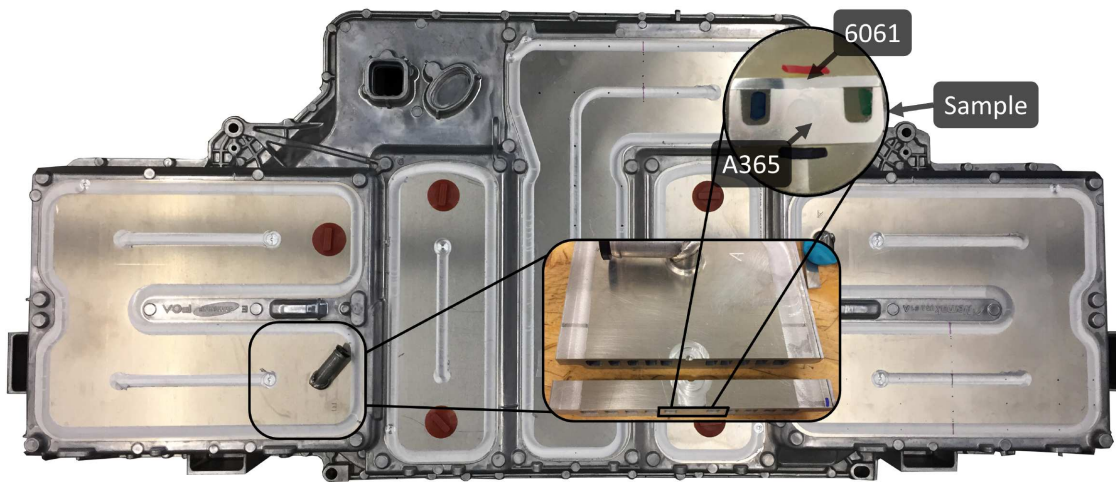


Figure 54: Location of metallurgical sample

Additionally, electron backscatter diffraction (EBSD) was used for grain orientation characterization. The EBSD procedure requires separate samples due to the extra precision necessary for reasonable grain detection rates. The EBSD sample followed the same polishing procedure as the SEM samples. However, following the last polish stage, a vibrating polisher with colloidal silica blue (10 pHs) as the suspension was used to reduce scratch sizes to 0.06 microns.

3.1.1.3 Neutron Diffraction Equations and Preparation

Neutron diffraction experiments were conducted in the High Flux Isotope Reactor facility at Oak Ridge National Laboratory on the HB-2B High-Intensity Diffractometer for Residual Stress Analysis (HIDRA) instrument. The specific methods utilized to calculate residual stress are

extensively discussed in the section “2.4.3.2 The Peak-Shift Method in the Calculation of Residual Stresses”.

3.1.2 Stress-Free Comb

The scanning was conducted in a 400x400 mm² area at the bottom left of the battery tray (see Figure 53), where the HIDRA instrument could be orientated to obtain all three directions of strain required for stress analysis. As the scanning occurred over several FSWs, stress-free samples that encompassed each weld were required (see Figure 55). The sample used to measure the data in Figure 56 contains the d -spacing profile for the comb stress-free sample, where a notable change in the d_0 value across the sample is established, corresponding to the physical operations during welding.

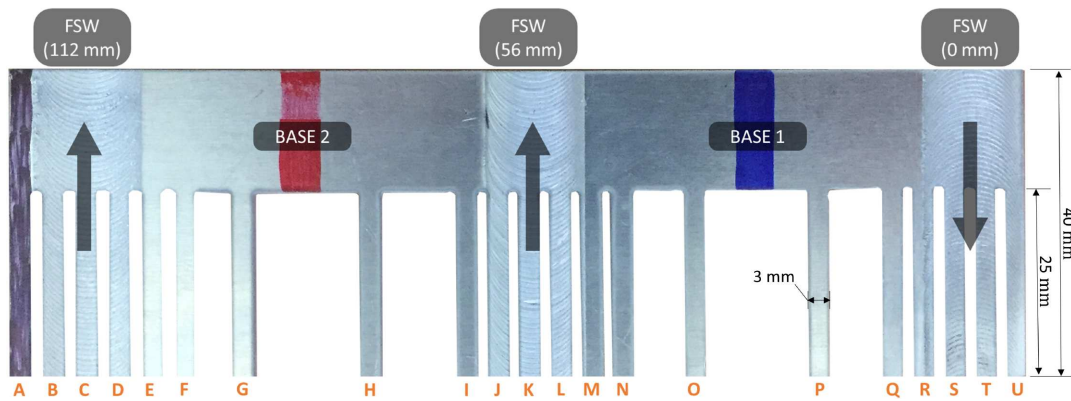


Figure 55: Stress-free comb sample and the direction of the tool during welding shown with the black arrows

For example, in Figure 56c, the stress-free d -spacing profile of the 56 and 112 mm weld is similar, and in both welds, the FSW tool is rotating and traversing in the same direction (see black arrows). However, at the 0 mm weld, the profile is a vertical mirror of the 56 and 112 mm profiles, corresponding to a change in tool rotation and direction. The changing d_0 value across the comb stress-free sample is due to the welding action changing the chemical composition across the weld (see Figure 56). This varying d_0 parameter across the weld line was also observed by Peel et al. [11], stating that significant errors in inferred residual stress would occur if a single value of d_0 were used for a diffraction-based experiment. The varying chemical composition that contributes to the changing d_0 throughout the sample is influenced by the strain rate, heat input, cooling rate, and direction of movement for the plasticized material during the FSW operation. On a processing

level, these factors are primarily controlled by the geometry, direction, and speed of the rotating and traversing FSW tool.

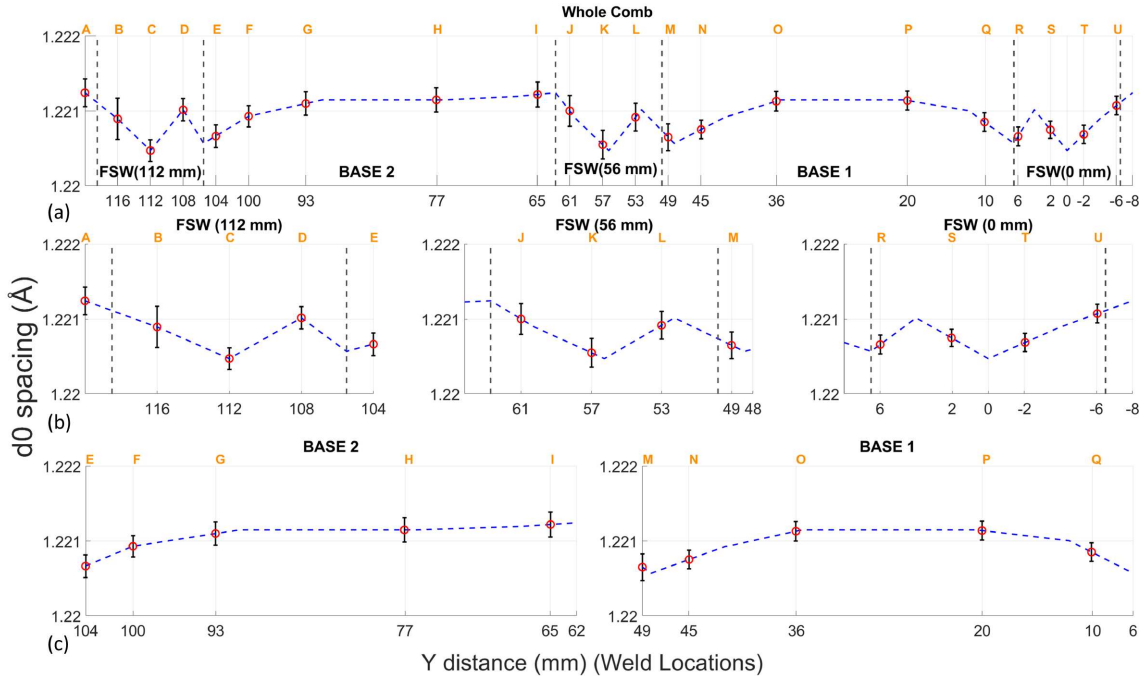


Figure 56: (a) Stress-free comb sample data; (b) Isolated welds; (c) Isolated base

3.1.3 Residual Stress

The stress measurements were obtained from the locations indicated in Figure 57a for the left wing of the battery tray (the blue highlighted region in Figure 53), where three orientations of stressed d -spacing could be measured. The scan on the left wing includes a line at -81 mm, shown in blue, a red scan line at 0 mm, and a yellow scan line at 81 mm (see Figure 57a and Figure 57b). The grey dotted planes in Figure 57b correspond to the weld location at 0, 56, and 112 mm from the component reference in Figure 57a. The 3D stress profile is shown in Figure 57b, and the corresponding projected 2D profile is shown in Figure 57c. This data presentation method is utilized throughout chapter 3 of this dissertation.

Moreover, this section presents data for the longitudinal-X, transverse-Y, and normal-Z directions (see Figure 57a for the directions), corresponding to residual stress measured in the X-direction, Y-direction, and Z-direction. The stress measurements are spaced out a few millimeters from each other, crossing three friction stir welds from 0 to 112 mm in the Y distance for the images in Figure 57. This Y distance spacing corresponds to the arrangement of the stress-free sample so that for

each location, the stress-free data can be applied. For example, the stress-free measurements are applied directly for the yellow and red lines, shown in Figure 58b; as these lines traverse over the three welds at 0, 56, and 112 mm, the exact layout situation in the comb stress-free samples. However, only the first two welds at 0 and 56 mm intersect the blue scan line, as there is no weld at the 112 mm mark for the blue scan line (see the black circle in Figure 58b). Therefore, unwelded 6061 stress-free d -spacing values are used in the outlined region shown in Figure 58b. Because the blue line does not intersect the weld, the maximum measured tensile stress is not on the 112 mm location; instead, high tension is in the location outlined in black for Figure 58a. In comparison, the red and yellow scan lines that intersect with welds measure maximum tensile stress directly adjacent to the weld lines and decreased tensile or compression stress outside the direct weld zone (see Figure 59). In the case of FSW, after part of the body's temperature is raised to welding temperatures and then starts to cool, there are significant differences in the cooling rate throughout the body. The difference in cooling rates experienced by the surface and depth of the body results in local differences in thermal contraction.

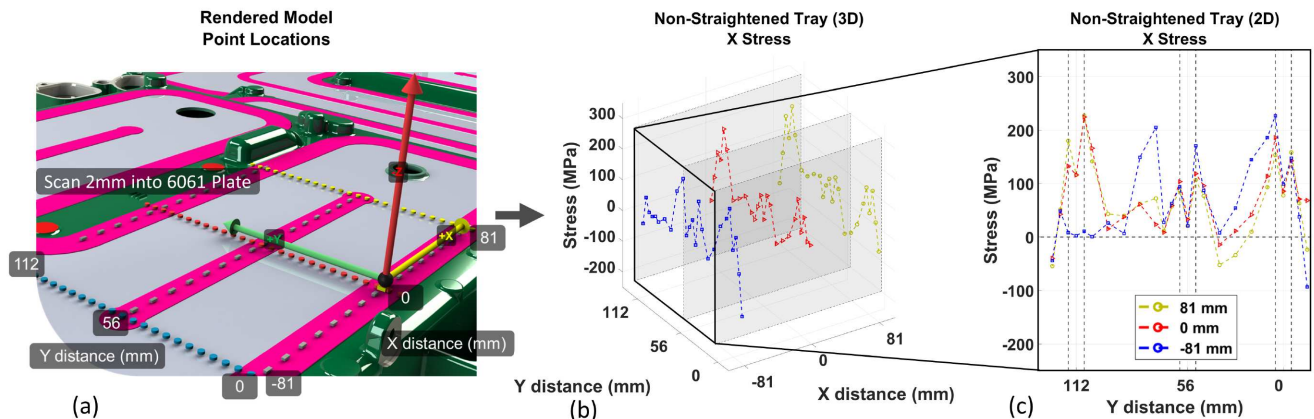


Figure 57: (a) Visualization of scan point locations; (b) Three-dimensional view of X-direction stress data; (c) Two-dimensional view of X-direction stress data

The formation of the X-direction stresses is explained in detail in section “2.3.2 Evolution of Residual Stresses in FSW”, referring to the results of Richards et al. [7] predicting the formation of longitudinal-X direction stress in Figure 24. As concluded in the description referred to above for X-direction stress development, any residual stress generated above the yield stress will result in plastic deformation of the material.

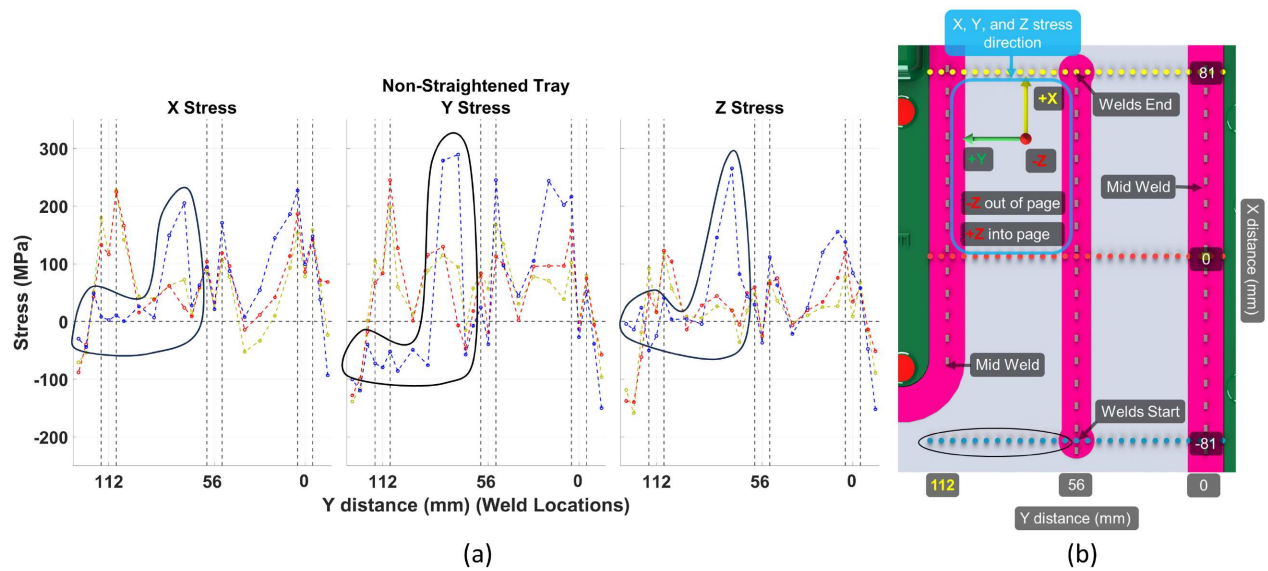


Figure 58: (a) X, Y, and Z stress directions; (b) Top view of scanned location highlighting the portion of the blue scan line that does not intersect a weld

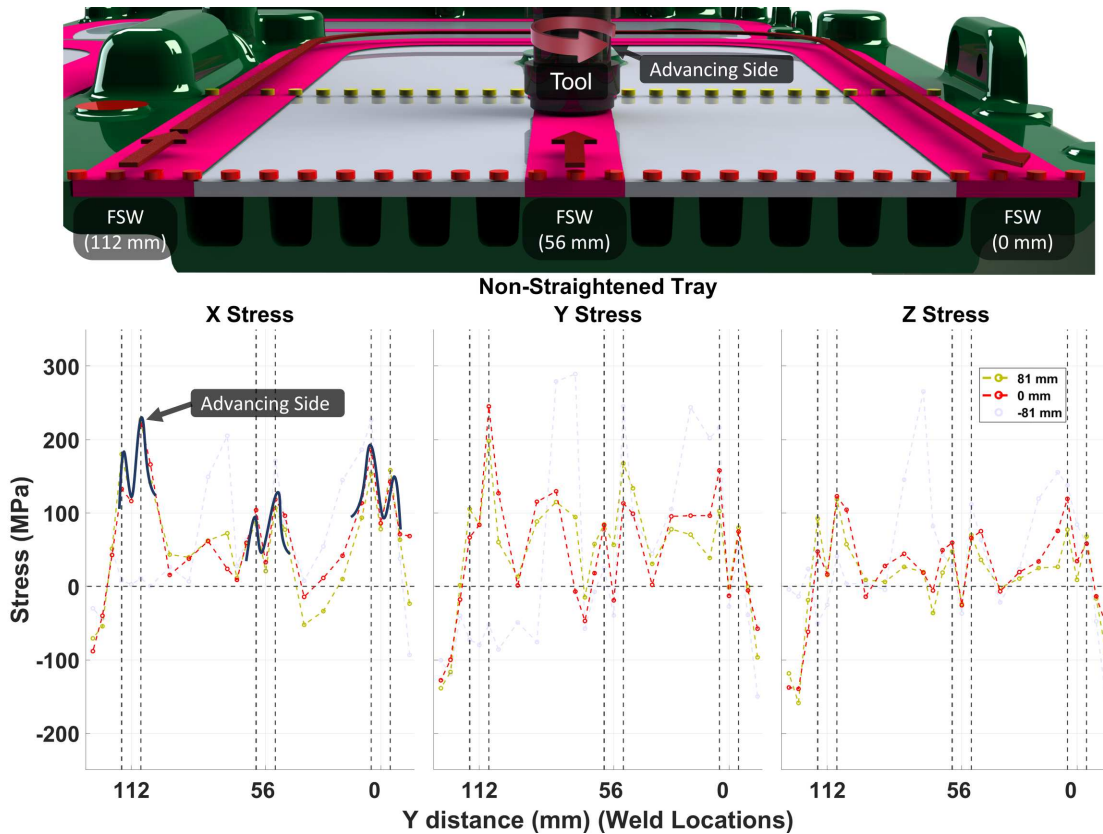


Figure 59: Measured residual stress profile (red and yellow scan lines)

Additionally, considering this process's dependence on temperature, a situation may arise where the weld that is conducted at 1.5 times the rotational speed (56 mm weld), resulting in higher temperatures (see Equation 1) and strain rate, inducing more plastic deformation into the material compared to the welds at slow rotational speed (0- and 112-mm weld). If significant strain-hardening effects were found in the 56 mm weld through hardness testing, this could support the theory of increased plastic deformation; this is considered and measured in the section “3.2.2 Microhardness”. Nevertheless, in all three weld cases, the residual stress builds up through thermal stress evolution during cooling. At this point, the uniformity and rate of cooling will determine how much thermal-generated residual stress is measured as yield stress increases with decreasing temperature. If the yield stress returns to a similar point and thermal contractions are comparable, then the residual stress is expected to be the same for each of the three peaks at 0, 56, and 112 mm welds, regardless of the process parameters. However, this is not the case, as seen by the 56 mm weld in Figure 59, with substantially lower residual stress than the 0 and 112 mm welds; this may come down to the order of operations. For example, consider the sequence order of welding: first conducted is the 56 mm weld; after completion, the tool is retracted and plunged into the peripheral weld path line (see Figure 53) that includes the 112 and 0 mm welds connected in a bend around the tray (see Figure 59). The 56 mm weld has already started to cool, gaining back more yield strength and allowing residual stress formation with thermal contraction. Now that the tool is welding and generating heat at the peripheral bend path for the 0 and 112 mm welds, that heat conducts through the aluminum and relaxes the residual stress in the 56 mm weld. James et al. [162] describe a weakly time-dependent temperature mechanism that causes the relaxation of the residual stresses; in the case of the aluminum battery tray cooling down to room temperature, this mechanism should be considered. The mechanism describes the relief of elastic strain through rapid thermal activation of dislocations in the presence of a temperature environment high enough to cause a sizeable reduction in the material yield strength. This mechanism necessitates that the yield strength approaches the value corresponding to the residual stress measured. The implication is that in the case of the FSWs, the peripheral welding generated enough heat to reduce the yield strength of the 56 mm weld to the measured residual stress state in Figure 59 to allow the thermal activation of dislocations to occur and redistribute to that yield strength. However, this topic is explored in greater detail in the section “4.2 Influence of Multiple Welds on Residual Stress and Distortion in Friction Stir Welding: A Neutron Diffraction Study”.

Regardless, considering the results of this section, Figure 63 reveals some highly distinctive profiles that have not been previously documented in the literature. Notably, the longitudinal x-stresses are not the highest recorded stresses. At the 112 mm position in the y-stress (transverse) direction, the stresses are of comparable or higher magnitudes. Additionally, the z-stresses, often considered negligible or in the non-zero plane stress condition, are significant, reaching up to 100 MPa at the 112 mm and 0 mm positions. These significant values indicate that friction stir welds on lap joints with complex components in dissimilar aluminum welding should not overlook the cast-component side, where initial stresses, such as those from high-pressure die casting, may influence the overall stress profile. This is important because operational stresses are distributed in all directions. Typically, directions with lower stresses compensate, but if high stresses are present in all directions, it may not lead to the most optimal distribution of operational stresses.

3.1.4 Microstructure

The grain refinement in the friction stir welded microstructure transpires during mechanical stirring in the dynamically recrystallized zone (DXZ). The tool pin causes intense plastic deformation in the depth of the material, which is pulled up to the tool shoulder from the retreating side of the weld and deposited on the advancing side, creating a unique marker signifying the advancing side shown in Figure 60. The microstructural advancing side marker corresponds to the residual stress peak on the advancing side of the tool, as shown in Figure 59.

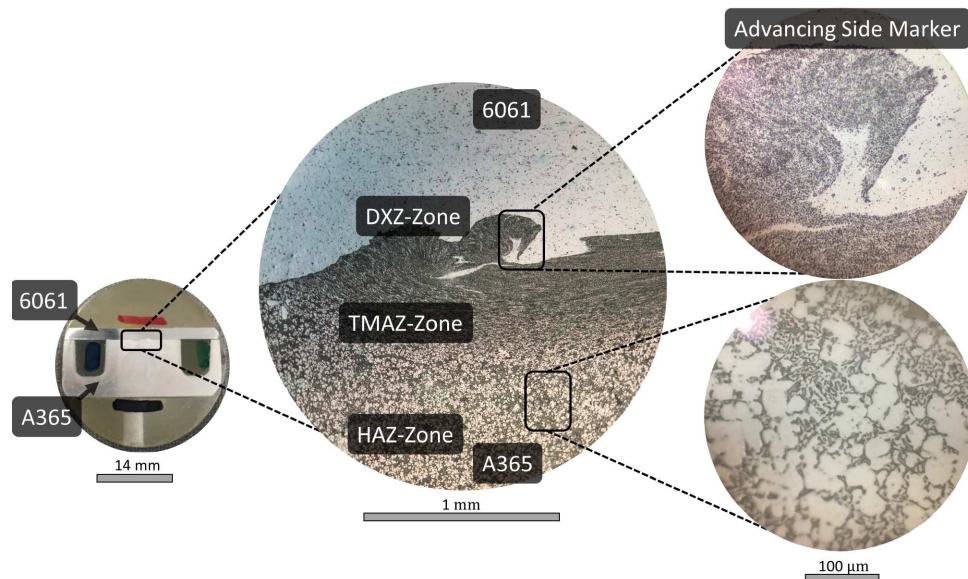


Figure 60: Friction-stir-weld zones identified with an optical microscope

Moreover, the resulting material flow creates the microstructure shown in Figure 60, where the DXZ results in a reduced magnitude of grain sizes that are more refined compared to the thermomechanically affected zone (TMAZ), heat-affected zone (HAZ) and base-metal. Fundamentally, the grain refinement depends on strain rate and thermal conditions, primarily determined by the tool rotation and traverse speed (Mishra et al. [53]). However, as discussed in the literature review, there are competing mechanisms between the rotational speed and the traverse speed that keep the grains relatively consistent with increasing welding speeds at a constant speed ratio.

Several optical microscope images are taken progressively into the material to confirm the end of the TMAZ and to view the possible formation of new phases at the weld interface, signifying possible melting during FSW. The first micrograph in Figure 61 is the 6061 material, taken 1.8 mm away from the reference point (surface of the material), as shown in the macro image of Figure 61. The next micrograph contains the 6061 and A365 cast interface, 2.0 mm away from the 0.0 mm reference. The small black and white phases are Al-Si eutectic, and the solid white grains are primary Al. The generated heat and "mixing" from the TMAZ recrystallize and deform the undistorted microstructure of the A365 until the 2.9 mm mark. After the 2.9 mm mark, the tool's interactions with the above material have no visible effect on the A365 microstructure, signifying the end of the TMAZ and the start of the HAZ.

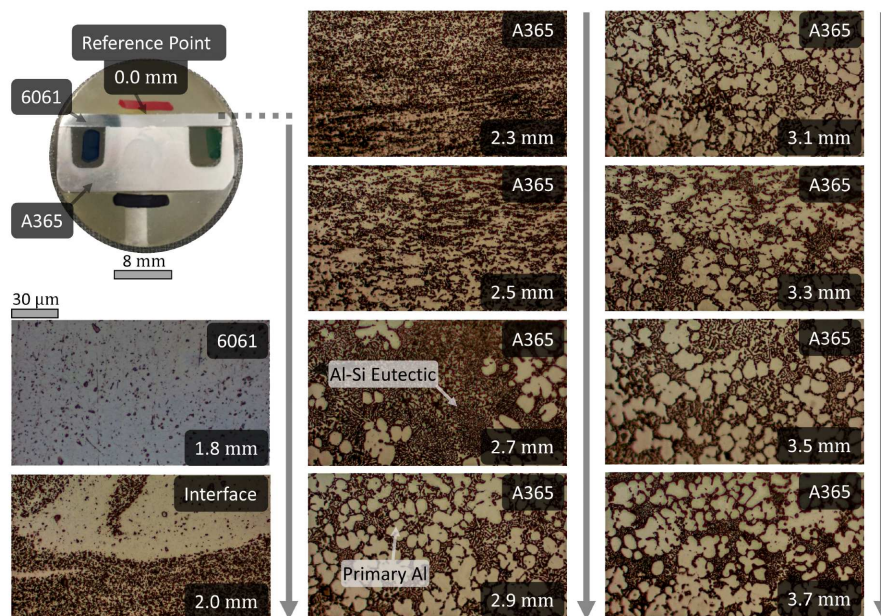


Figure 61: Stepping optical microscope images (all micrographs taken at 100x magnification)

The interface image at the 2.0 mm position in Figure 61 shows no formation of phases between the A365 and 6061. This lack of interfaced phases is supported by isolating a dominant element in one of the materials. The silicon (Si) in the A365 (9.73 Si wt%.) material is in high concentration as compared to the 6061 (0.70 Si wt%.) material. The Si in the A365 is tracked utilizing SEM and Energy-dispersive X-ray Spectroscopy (EDS) by identifying the K-alpha emission lines unique to Si (Goldstein et al. [163]). In Figure 62, the high Si content is strictly contained in the A365 material. Even in the heavily recrystallized areas where the A365 grain size is most refined, the 6061 and A365 materials do not mix into a material with a new chemical composition. This lack of mixing suggests that the plasticized pieces during the FSW operation strictly stir the material and do not mix, leading to a hard transition between 6061 and A365. This observation further reinforces that the operation conducted for this experiment was in a solid state, where the material did not transition into liquid form. While true for the welding occurring on the battery tray, this no-melting observation may not be valid for all FSWs, as process parameters can be adjusted to increase temperature and achieve melting in the DXZ.

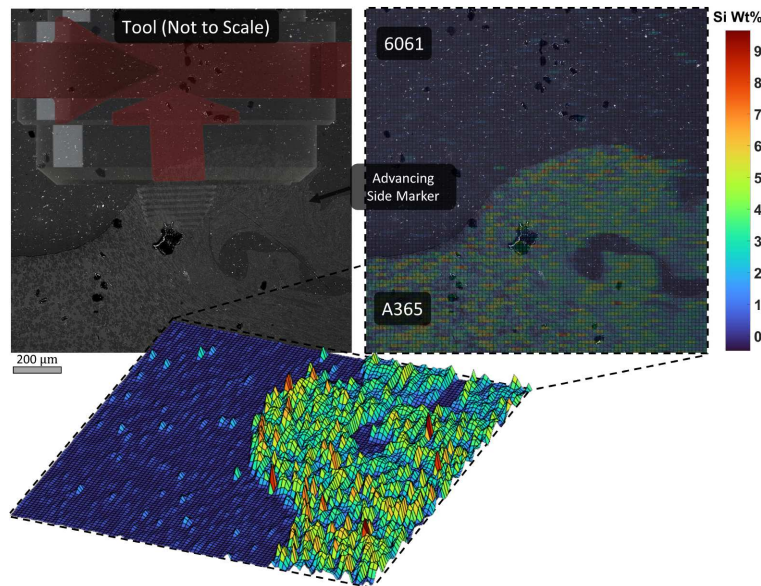


Figure 62: A365-6061 silicon transition

3.1.5 Section Conclusions

Using the widely studied 6061 and A365 Al system, the effect of the welding conditions, including the material position, microstructure, and resulting residual stress in a wrought-to-cast lap FSW,

has been demonstrated and characterized using neutron diffraction and optical imaging techniques. Within the range of the welding conditions used, the conclusions are as follows:

- (1) The tool's rotation and transverse direction, with respect to the geometric constraints of the component, influence the residual stress profiles. For example, if the rotational and traverse direction switched, the stress profile would mirror along the vertical axis, with the maximum tensile stress consistently occurring on the advancing side of the tool rotation direction.
- (2) Unintentional preheating of the battery tray by welds occurring in the proximity of unwelded locations results in a decrease of maximum tensile stress after these unwelded locations become welded.
- (3) Microstructural marks and residual stress results indicate that the advancing side of the tool rotation is consistent with the maximum residual stress location, where the advancing side's maximum stress can be 70% higher than the tool's retreating side.
- (4) The EDS analysis of a weldment portion reveals the maximum silicon content through the 6061 to A365 FSW interface, suggesting that the plasticized pieces during the FSW operation primarily stir the material with minimal mixing, leading to a very narrow transition zone, on the order of 100 microns.

Before proceeding to the stress analysis in the section "3.3 Analyzing Distortion and Stress Effects in Response to Straightening the Battery Tray," further microstructural evaluation is completed in the next section. This evaluation includes hardness testing, electron backscatter diffraction (EBSD), and inductively coupled plasma atomic emission spectroscopy (ICP-AES) to compare the structure and chemical composition between the welds of the battery tray that utilize different welding parameters.

3.2 Microstructural Changes in Friction Stir Welds of the Battery Tray

The near-pin welding results in a dynamically recrystallized zone (DXZ) with reduced grain sizes that are more refined compared to the thermo-mechanically affected zone (TMAZ), heat-affected zone (HAZ), and base metal (BM). Slightly away from the pin and shoulder lies the TMAZ region. Since aluminum can withstand significant plastic strain without recrystallization, the welding action creates a distinct boundary of elongated grains between the DXZ and the HAZ, known as

the TMAZ. The friction stir welding (FSW) process significantly changes the microstructure through heat and plastic deformation but also alters the type and density of the precipitates in the weld regions.

For instance, when joining A365 and 6061 alloys, the heat and plastic deformation soften the material, causing the disappearance of precipitates. Although aluminum 6061 contains many strengthening precipitates (e.g., GP-I zones, GP-II zones or β'' , and β'), the high density of fine needle-shaped β'' precipitate is the primary strengthening mechanism for AA6061-T6. Therefore, the type, size, and distribution of precipitates are particularly important to the mechanical properties of the friction stir weld joint. High-strength Al alloys consist of high solute levels, and several phases depend on the thermomechanical history. This strengthening mechanism is susceptible to welding conditions, as these conditions dictate the local temperature and the heating/cooling rates during welding.

However, many factors that control the resulting microstructure and properties in friction-welded joints are inadequately understood. A study by Dong et al. [164] discussed the post-weld natural aging of FSW 6005A-T6 Al alloy. They found a loss in hardness for the DXZ and TMAZ an hour after welding, attributed to the loss of the original β'' precipitates due to high temperatures and non-optimal cooling rates. However, further post-welding hardness testing revealed a hardness recovery mechanism attributed to the formation of independent Mg and Si clusters (on day one) and GP zone precipitation (by week four of natural aging). Another study by Li et al. [165] described the butt-configured joint of AA6061-T6 sheets utilizing stationary shoulder FSW and found that small, dotted GP zones mainly represent the DXZ region, indicating the high density of fine needle-like β'' precipitates, the main source of strengthening, were not present in the DXZ. The β'' precipitates had dissolved and reformed as GP zones during cooling.

GP zones, or Guinier-Preston zones, are very small, coherent clusters of solute atoms that form in the early stages of precipitation hardening in aluminum alloys like 6061. These zones are typically a few nanometers in size and form at lower temperatures before the main strengthening precipitates, such as the beta prime (β') precipitates, develop. In 6061 aluminum, the beta prime double prime (β'') precipitates are the primary source of strength, providing significant hardening by hindering dislocation movement. However, during processes like welding, these β'' precipitates

can dissolve due to the high temperatures involved. As the material cools, these dissolved precipitates can reform into GP zones instead of returning to their original β'' state.

The formation of GP zones instead of β'' precipitates can reduce the alloy's strength. Although GP zones do contribute to some strengthening, they are less effective than β'' precipitates because they provide less resistance to dislocation movement. Therefore, the transition from β'' precipitates to GP zones generally results in a decrease in the overall strength of the material. GP zones can be considered as precursors to the more effective β'' precipitates, but on their own, they do not provide the same level of hardening.

This raises the question of whether changes in hardness are not solely due to the formation or lack of precipitates but could also be influenced by texture-oriented mechanisms, such as texture-induced grain coarsening. Increasing the welding speeds may reduce these effects by randomizing the texture or causing more misorientation, leading to better mechanical properties.

The effects or contributions of plastic deformation with different welding parameters on the resulting mechanical property distribution are seldom discussed in the literature. Since the torque required to rotate the tool inside the material and generate intense plastic deformation depends on a combination of the plunging force, the coefficient of friction (sliding friction), and the flow strength of the material neighboring the tool (sticking friction), these forces and thermal input are responsible for deformation in the workpiece material (Singh et al. [166]). These inputs subsequently affect the resulting mechanical properties of the weld.

Therefore, this section evaluates the mechanical properties and microstructural characteristics of the battery tray welds. This analysis is accomplished using Vickers hardness testing to identify hardness properties across each weld. Electron backscatter diffraction (EBSD) was utilized to determine the plastic deformation and texture differences across each weld. Finally, inductively coupled plasma atomic emission spectroscopy (ICP-AES) was utilized to account for accurate compositional differences across each weld.

3.2.1.1 EBSD and ICP-AES Spectrometer Sample Preparation

EBSD was conducted on a metallurgical sample extracted from the top surface of the 0-, 56-, and 112-mm weld strictly from the 6061 material. First, the sample was mounted and polished following the principles outlined in the ASTM E3-11 standard. Then, to ensure reasonable grain detection rates for EBSD, the sample surface was prepared via a vibrating polisher, with colloidal silica blue (10 pHs) as the suspension, for 4-5 hours. The sample preparation for inductively coupled plasma atomic emission spectroscopy (ICP-AES) requires the dissolving of a 0.1 g sample of aluminum through heating and NaOH to dissolve the Si in the alloy, followed by the addition of peroxide (H_2O_2) to clear dissolved gas. Then, a mixture of HNO_3 and HCl is added to complete the digestion and retain the dissolved elements in the solution. Finally, dilution of the solution is required to finalize sample preparation for analysis (Perkin Elmer et al. [167]).

3.2.1.2 Microhardness Sample Preparation

The Vickers hardness testing is conducted on a metallurgical sample extracted from the battery tray extending across each of the welds, as shown in Figure 63. First, the sample was polished to increase the contrast between the indents and material, then mounted in the sampling stage and tested with a load of 500 g for a 10-second duration, following the principles outlined in the ASTM E384-17 and ASTM E92-17 standards for Vickers micro indentation testing.

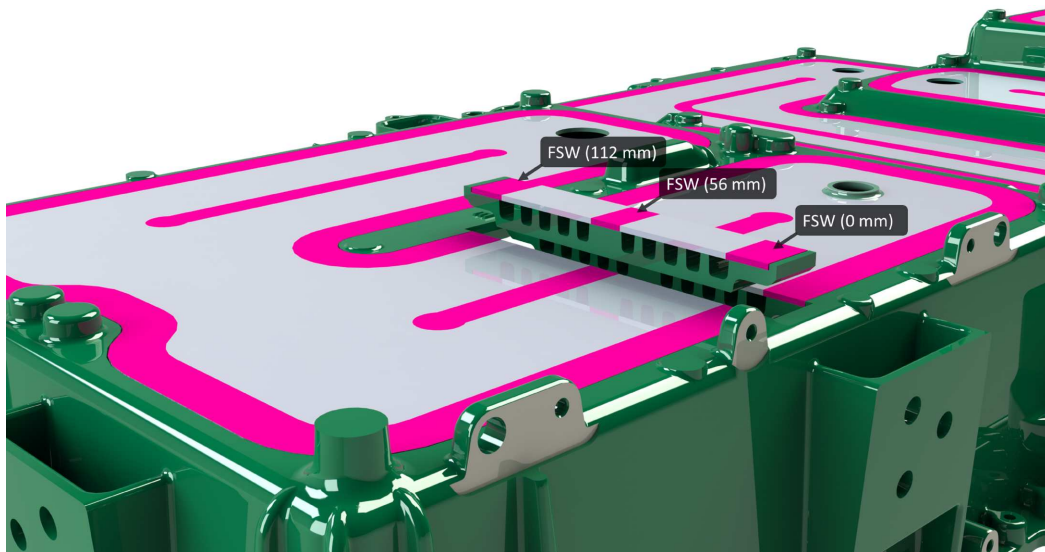


Figure 63: Hardness sample location across each friction stir weld

3.2.2 Microhardness

Several micro-hardness profiles were created utilizing the indent measurements across the three welds, FSW (0 mm), FSW (56 mm), and FSW (112 mm), along the surface and into the depth of the material (see Figure 64). The top surface data in Figure 65 contains relatively low hardness values for the 112- and 0-mm weld, ranging between 90 and 80 HV. In contrast, the middle 56 mm weld reaches 105 HV on the top surface. This variation in hardness between the 56 mm weld and the 112- and 0-mm weld may be attributed to the significant increase in rotational speed utilized for the 56 mm weld. Similar to the present paper, the study by Li et al. [165] on AA6061-T6 using stationary shoulder FSW observed a significant decrease in hardness at the DXZ, with the most severe softening to 50–75 HV for the lowest tested rotational speed (750 RPM). However, as the rotational speed increased at constant welding speeds, the hardness in the DXZ reached nearly 80 HV for a rotational speed of 1500 RPM. This hardness magnitude is consistent with the 0- and 112-mm welds tested at similar rotational speeds for the research presented in this dissertation. Additionally, the study by D. Li et al. [165] states that more strengthening precipitates are generated at high tool rotation speeds, supporting the result of increasing hardness as the rotational speeds increase. Yet, in this study, the maximum measured hardness for the greatest rotational speed on the 56 mm weld is only near-surface, as seen by the maximum peak on the 0.1 mm and 0.15 mm curves for the 56 mm weld in Figure 65. However, further down into the material, at 0.6 mm and 1 mm, the hardness values remain relatively consistent along the entire measurement line between the 112-, 56-, and 0-mm weld (see Figure 65). The near-surface hardness variations may suggest an entirely different mechanism contributing to hardness increasing with increasing rotational speeds. For example, it is known that aluminum has excellent heat dissipation properties, creating a consistent temperature environment throughout the 1 mm depth directly below the tool (Andrade et al. [168]). Therefore, the generation of precipitates should be consistent near the surface and at 1 mm depth. However, what is inconsistent is the amount of applied plastic deformation by the tool. Plastic deformation occurs near the material's surface during FSW. Therefore, if higher rotational speeds and plunging forces are utilized, the material may experience more plastic deformation and grain randomness, increasing hardness at the surface. This may explain why the 0.6 mm profile has a consistent hardness profile below the surface throughout the three welds conducted at vastly different rotational speeds. This concept is further investigated and supported by EBSD work performed for this section on the identical welds tested for hardness.

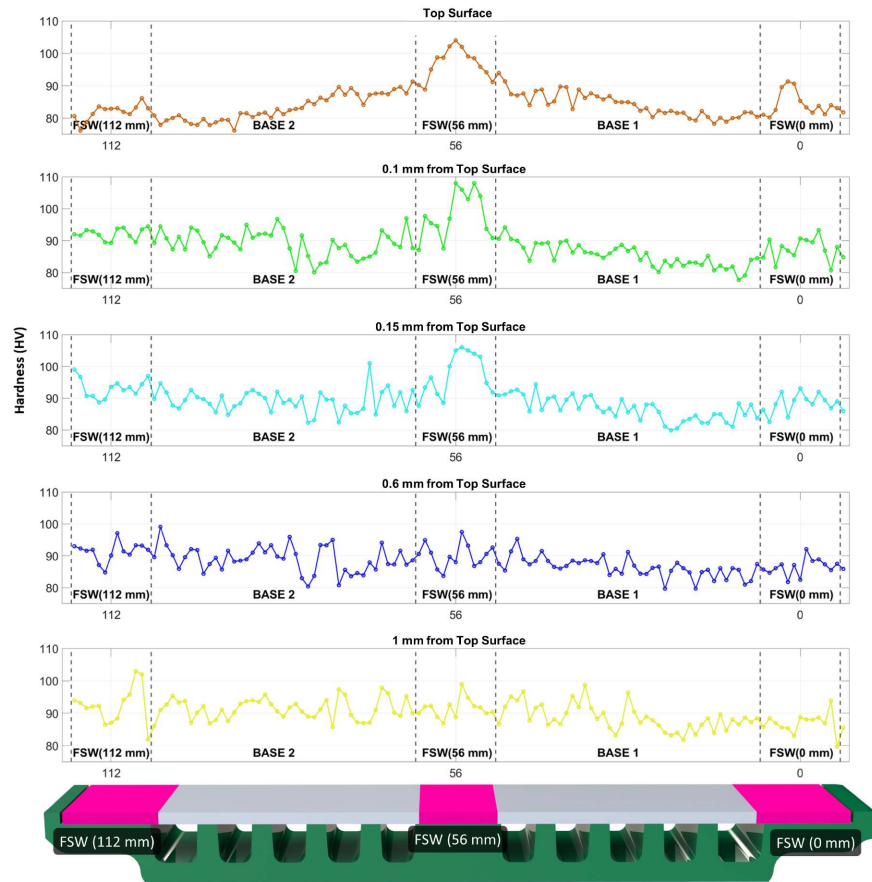


Figure 64: Location of each hardness profile across and down the surface of the friction stir weld

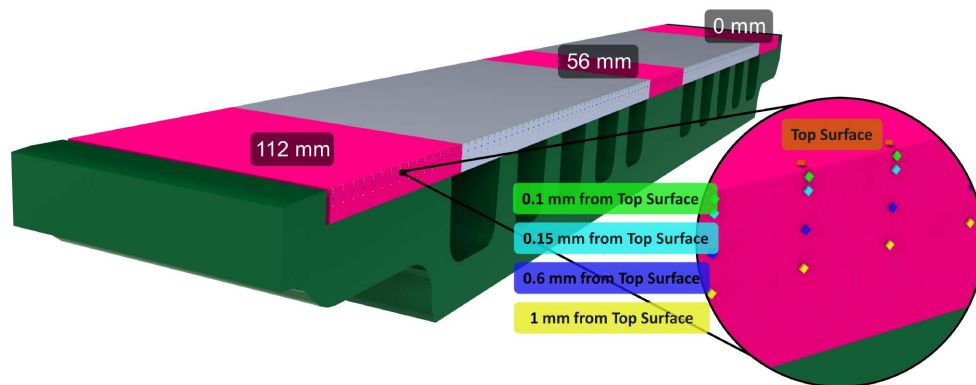


Figure 65: Measured hardness across and down each friction stir weld

3.2.3 EBSD

Analysis with EBSD is based on the band contrast map created by the beam's interactions with the crystal structure. These interactions generate Kikuchi patterns or bands detectable through EBSD

(Nishikawa et al. [169]). The positions of the Kikuchi bands determine the interplanar angles, while their widths determine the interplanar spacings. To index the grain, the angles and widths of the Kikuchi bands are compared with theoretical values known in advance for the actual crystal structure, in this case, aluminum face-centered cubic (FCC). Only the first three families of $\{hkl\}$ planes are required to solve a backscatter Kikuchi pattern for FCC due to its cubic and high crystal symmetry. After each grain is indexed, the crystallographic orientation or misorientation is described by $(hkl) [uvw]$ notation or by three Euler angles (ϕ_1, Φ, ϕ_2).

EBSD deformation analysis employs grain pattern rotation maps referencing local misorientation, average misorientation, or quantifications through calculated dislocation densities. These methods depict plastic deformation around defects, cracks, or distorted grains in polycrystalline materials. Bulk dislocations that accumulate in the lattice during plastic deformation are indirectly detectable through EBSD. This is because EBSD observes the changes in the polycrystals resulting from plastic deformation. These changes are detected by measuring grain misorientation as a spatial gradient in orientation, which is then related to lattice curvature (deformation) through the Nye tensor.

The Nye tensor is a mathematical construct used to quantify the density of geometrically necessary dislocations (GNDs) in a crystalline material. It approximates the GND density by measuring variations in the orientation of individual crystal grains obtained from EBSD data. Essentially, more estimated GNDs indicate more plastic deformation. It is essential to note that misorientation must be relative to a reference orientation, and choosing this reference can drastically change the visualization of plastic deformation. For example, if only a highly deformed section of the material is measured, it would not appear deformed on the misorientation map if the reference is another equally deformed grain. Therefore, a large area indicative of both non-deformed and plastically deformed material is measured for an accurate assessment.

One effective way to produce a misorientation map is by kernel average misorientation (KAM) (Schwartz et al. [40]). KAM calculates the arithmetic mean of the scalar misorientation between groups of pixels and maps them. The KAM map provides a visual representation of local misorientation, which can imply the presence of GNDs but does not directly quantify them. The relationship between misorientation and GNDs is indirect; higher local misorientation often

corresponds to higher densities of GNDs, but they are not equivalent. This map is produced for the three welds in Figure 66. In Figure 66, a comparison of angular grain misorientation using the KAM map is made between each weld. The HAZ shows low misorientation for each weld, signified by the blue-coated grains. The green-coated grains illustrate that the TMAZ has a misorientation of 12 degrees for each weld. Finally, the region directly in contact with the tool shoulder is the DXZ, showing the highest misorientation.

The KAM map measures significant grain misorientation in the DXZ, indicating plastic deformation throughout each weld. However, the 56 mm weld has significantly higher surface hardness (see Figure 64), indicating more plastic deformation; therefore, grains should be at higher misorientation in the KAM map. Despite this, the visual distinction between the 56 mm weld and the 0- and 112-mm welds is difficult to see on the KAM map. To address this, the center of each weld, the area within the yellow box in Figure 66, is quantified to analyze and compare the welds numerically (see Figure 67).

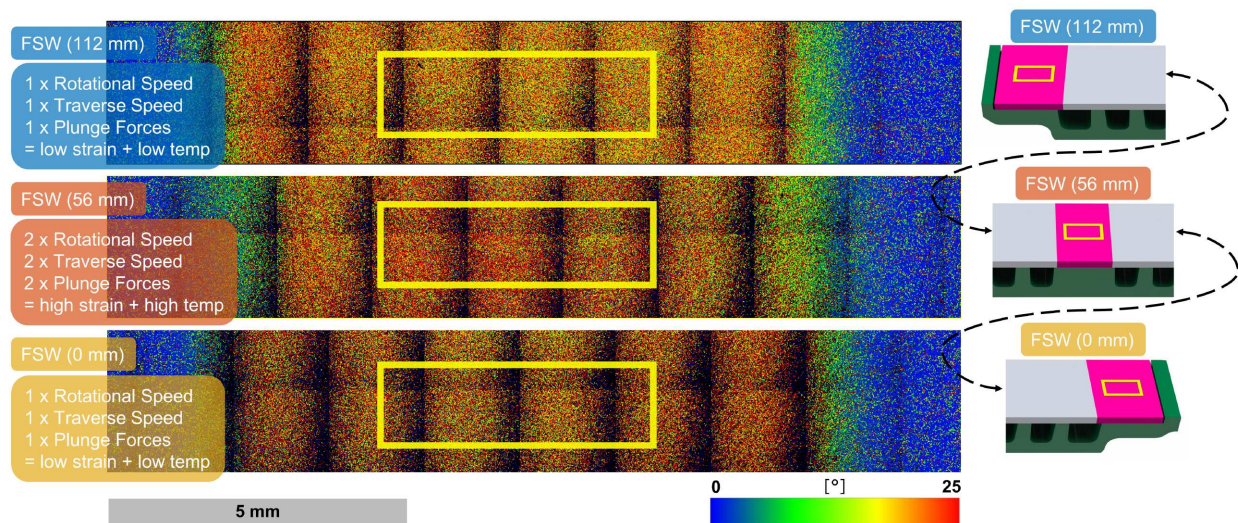


Figure 66: KAM map of each weld along with the quantified region (yellow box)

In Figure 67, the grains are grouped into bins, each containing grains with a specific range of misorientation degrees. For example, the fraction of grains with low-angle misorientations, between 4.5 and 28.5 degrees, is dominated by the 0-mm and 112-mm welds. In contrast, the fraction of grains with high-angle misorientations, between 34.5 and 58.5 degrees, is predominantly represented by the 56-mm weld, which was conducted at 1.5 times the rotational

and traverse speed with nearly twice the plunge force. Therefore, the 56-mm weld likely has a higher density of GNDs due to significantly more plastic deformation and higher surface hardness.

Furthermore, severe plastic deformation can be considered the primary source of texture variation during friction stir welding (FSW). If the crystallographic texture is significantly different between the 56-mm weld and the 0-mm and 112-mm welds, this would illustrate the increasing plastic deformation with increasing rotational speed. Moreover, the mechanical properties between the welds may also be severely affected depending on the final crystallographic texture.

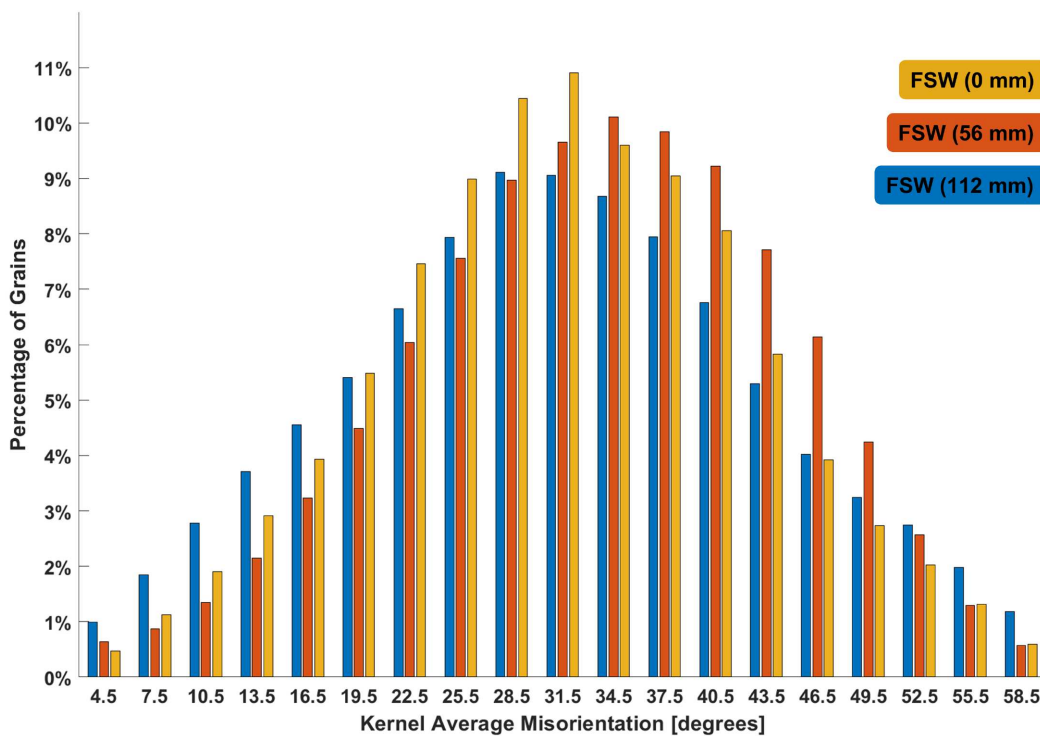


Figure 67: Quantified region of the KAMA map (yellow box from Figure 66)

Fortunately, EBSD texture analysis is readily available through the use of the inverse pole figure (IPF) map. The IPF map color codes specific crystal planes perpendicular to the rolling, transverse, or normal axes (see Figure 68b). The color code key is based on the cubic crystal's stereographic projection (see Figure 68a), which retains only the [001], [101], and [111] directions from the aluminum crystal. A specific axis is chosen, and all crystals on the IPF map are then color-coded depending on their perpendicular orientation to the selected global axis. For example, in Figure 68c, a small portion of the overall map is shown, where the selected global axis is the normal direction, and the crystals are color-coded based on which plane is perpendicular to the normal

direction. However, Figure 68c represents an ideal case where not all the crystals align perfectly with one of the chosen directions. Depending on a crystal's perpendicularity to the selected axis, it can be colored between one of the color key planes (yellow, magenta, or cyan – see Figure 68a). In the IPF map for the 0-mm and 112-mm welds in Figure 68, a strong ordered texture is seen across the weld, corresponding to the location of the pin where the $[101]$ planes are perpendicular to the normal direction. In the shoulder contact region, more of the $[001]$ planes are perpendicular to the normal direction. As the plasticized material adheres to the tool surface and is subsequently left behind, it is expected that the texture direction will change from pin to shoulder, resulting in different texture directions for these areas. Studies on flow velocity support this reasoning (Zhang et al. [170]). On the other hand, the IPF map for the 56-mm weld exhibits a much more unique texture with a laminar structure of many different planes layered over each other.

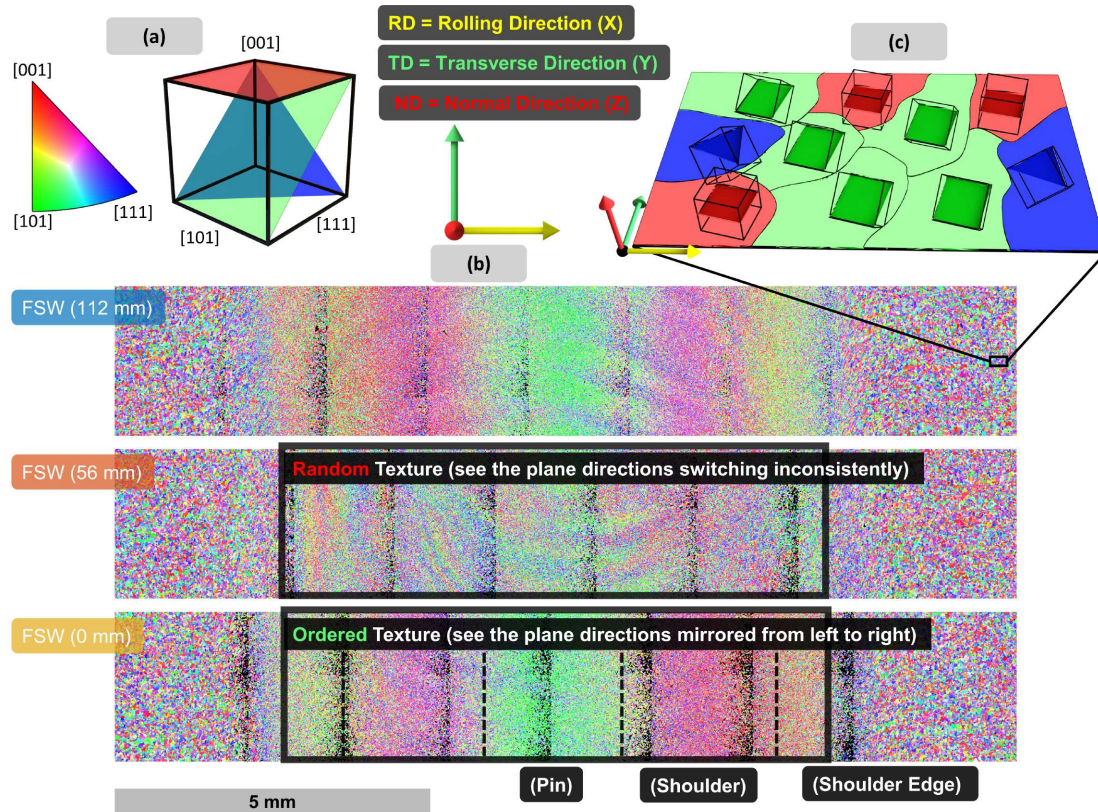


Figure 68: Inverse Pole Figure (IPF) map of each friction stir weld

This layer-textured structure may indicate changes in how the tool deposits the material during FSW. The coefficient of friction varies with temperature: at lower temperatures, the coefficient is higher, leading to more material sticking to the tool. As a result, the material remains on the tool's surface longer, depositing in an orderly fashion from one end of the weld to the other, as seen in

the 0- and 112-mm welds in Figure 69. Conversely, at higher temperatures, achieved with increased tool rotation for the 56 mm weld, the coefficient of friction decreases. This causes the material to slip off the tool surface more readily, leading to a more random, layered texture with different grain orientations. This texture is evident in the 56 mm weld in Figure 68, where the [111], [001], and [001] planes alternate along the weld path on the advancing side.

Supporting this, Nie et al. [156] found that increasing temperature reduces the coefficient of friction and increases the slip rate during FSW. The slip rate is highest at the edge of the shoulder, where the tool's velocity is greatest, and factors like applied force and welding speed also influence the slip rate. In FSW, "slip" refers to the relative motion between the tool and the workpiece at the interface, not the actual slipping of material from the tool. This distinction can be understood through the following conditions:

- Sticking Condition: When the contact at the tool-workpiece interface is fully sticking ($\delta = 0$), there is no relative motion between the tool and the workpiece. The material moves with the tool, and plastic deformation is the primary source of heat generation.
- Sliding Condition: When the contact is fully sliding ($\delta = 1$), there is complete relative motion between the tool and the workpiece. The tool slides over the workpiece, and friction is the primary source of heat generation.
- Partial Sticking/Sliding Condition: Typically, the contact state is between these extremes ($0 < \delta < 1$). Both friction and plastic deformation contribute to heat generation. The slip rate (δ) quantifies the degree of sliding versus sticking at the interface.

When the slip rate is high (closer to sliding), the material is more likely to slip past the tool rather than being carried along with it, affecting how the material is deposited back into the weld zone. This can lead to different microstructural characteristics and weld quality compared to a condition where sticking predominates.

Because of the increased temperature due to higher rotational speeds (see Equation 1) at the 56 mm position, more slipping occurs, causing a randomly oriented, layered structure with less overall texture. This random orientation is visible at the 56 mm weld position. Additionally, higher rotational speeds increase plastic strain (Feng et al. [171]; Hadi et al. [172]), potentially strain hardening the material. Another consideration is the reduction of texture-induced grain coarsening

in the randomly oriented structure at the 56 mm position. In comparison, large sections of the 0- and 112-mm welds are aligned, acting like coarse grains and affecting material properties. This alignment can reduce strength compared to the Hall-Petch strengthening described in 2.2.5.1 Texture Considerations. Therefore, the increased hardness observed in the 56 mm weld may result from the non-aligned grain boundaries, which impede dislocations more effectively than the aligned grains in the 0- and 112-mm weld positions (see Figure 65 for hardness distribution). The possible effects of texture-induced grain coarsening are supported by the fact that grain refinement is consistent between each weld position and, therefore would not contribute to any difference in hardness variation. The average grain size in the DXZ is practically the same across each of the weld positions: 13.14 microns for the 0 mm weld, 13.18 microns for the 112 mm weld, and 13.21 microns for the 56 mm weld.

This finding aligns with the literature discussed in the section titled “2.2.4 FSW Microstructures”, which suggests that maintaining a constant speed ratio (the ratio of rotational speed to traversing speed) results in relatively unchanged average grain size, regardless of the specific welding parameters. This constancy occurs despite increasing welding speeds. The maximum temperature increases with higher welding speeds, but the reduced thermal exposure time limits the grain growth during the dynamic recrystallization process. Thus, two competing factors are at play: the increased temperature from higher rotational speeds and the decreased grain growth time due to faster traversing speeds reducing temperature exposure.

3.2.4 ICP-AES Analysis

To further illustrate the differences caused by increasing rotational speed, the segregation of elements can be analyzed. The rotating threaded tool pin's dragging effect is known to concentrate elements within the weld (Attallah et al. [173]; Salem et al. [174]). If the rotational speed is significantly increased, this dragging effect should proportionally increase. However, as discussed above, the increase in heat can lead to more sliding of the tool on the material and less adherence, potentially reducing the dragging and accumulation of elements. This can explain the differences observed in certain measurements in this dissertation, as the welds may have different compositions due to the varying welding parameters used.

In this section, inductively coupled plasma atomic emission spectroscopy (ICP-AES) analysis is used to measure the concentration of elements within each weld with an accuracy of a few parts per million (PPM). ICP-AES detects elements using an emission spectrophotometric technique, which exploits the fact that excited electrons emit energy at specific wavelengths when they return to their ground state. Each element emits unique wavelengths based on its chemical nature, and the technique typically selects a single wavelength for detection. The energy intensity emitted at the selected wavelength is proportional to the element's concentration. By determining which wavelengths are emitted by a sample and their intensities, the sample's elemental composition can be precisely determined in reference to a standard.

For this analysis, the metals must be in solution, which is achieved by digesting the solid sample using a series of acids (Todolí et al. [175]). Before digestion, the A356 cast material was completely removed from the welded sections of the Al 6061 in the battery tray. The Al 6061 was then sectioned into nine samples, separating the welded area from the surrounding material (see Figure 69).

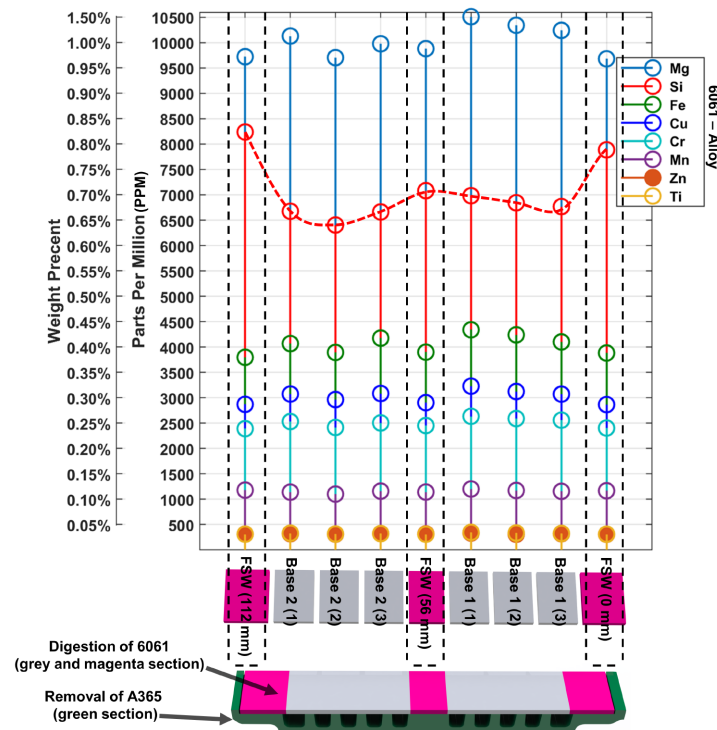


Figure 69: Element concentration differences between friction stir welds for aluminum 6061

Immediately evident in Figure 69 is the noticeable change in silicon (Si) concentration between the welds, while other elements, like copper (Cu), iron (Fe), and manganese (Mn), show visible but relatively minimal changes compared to the base material. These minimal changes in non-Si elements are likely due to differences in their diffusion rates in aluminum (Al) at welding temperatures. For example, at 500 degrees Celsius, the diffusion coefficient of Si ($1.5 \times 10^{-9} \text{ cm}^2/\text{s}$) in Al is much higher than that of Cu ($5.9 \times 10^{-10} \text{ cm}^2/\text{s}$), Fe ($3.9 \times 10^{-11} \text{ cm}^2/\text{s}$), and Mn ($7.5 \times 10^{-13} \text{ cm}^2/\text{s}$) in Al (Zhang et al. [176]).

Using Si as a reference point, it is evident that Si is concentrated in the weld (see Figure 69). This concentration mechanism can be attributed to the dragging and rotating action of the tool, as suggested by other researchers (Attallah et al. [173]; Salem et al. [174]). Furthermore, the degree of Si concentration differs comparing the 56 mm weld to the 0- and 112-mm welds (see Figure 69). This difference suggests that higher rotational speeds in the 56 mm weld lead to more sliding and less Si concentration because less material adheres to the tool surface. In contrast, at lower rotational speeds (0- and 112-mm welds), more material sticks to the tool, resulting in an ordered texture and higher Si accumulation in the weld (see Figure 68 for texture variations and Figure 69 for Si accumulation results).

Understanding the potential sliding and/or sticking differences with changing welding parameters is important when considering material flow during FSW. As noted by other researchers, the grip or stickiness of the tool shoulder on the plasticized material primarily determines material flow (Yeshitla et al. [177]). Although both sliding and sticking generate heat to soften the material, effective material flow is primarily caused by sticking. Therefore, to create a sound weld with good adhesion, there must be adequate adherence of plasticized material to the tool surface, utilizing the sticking mechanism. If the material does not stick, it deposits in an unordered, layered manner, potentially affecting the joint's effectiveness.

For example, a study by Arora et al. [178] suggested that the optimum shoulder diameter should correspond to the maximum sticking torque for a given set of welding parameters and workpiece material. Any further increase in tool rotation and traverse speed with a constant diameter could result in decreased grip of the tool on the material, leading to more sliding and less sticking.

3.2.5 Section Conclusions

In this section, friction stir welds joining several Al 6061 plates to the A365 HPDC structure were analyzed considering the different processing parameters used for the 56 mm weld (higher plunge forces, rotational speeds, and traverse speeds) compared to the 0-mm and 112-mm welds (half the plunge forces, with 1.5 times lower rotational and traverse speeds). These variations in processing parameters resulted in differing hardness properties for the two weld groups. The effect of these varied processing parameters on microstructural evolution and hardness properties was investigated in detail. Based on the observations and analysis, the following conclusions were drawn:

- (1) ICP analysis for both weld groups found distinct differences in elemental concentrations. The weld executed at 1.5 times the rotational and traverse speed exhibited a smaller concentration of silicon absorbed from the base metal, while the weld at the slower rotational and traverse speed showed a significantly higher concentration of silicon. This observation can be attributed to the slower traverse speed, which subjects the weld to heat for more extended periods, even at lower temperatures. As diffusion depends on both temperature and time, silicon has more opportunity to diffuse into the welds from the base metal when the traverse speed is slower. Therefore, the prolonged exposure to heat for the weld at the slower traverse speed likely leads to the annealing of typical strain hardening effects observed in welds, explaining the minimal difference in hardness between the base metal and these welds. In contrast, the weld performed at 1.5 times the rotational and traverse speed exhibits strain-hardening effects due to shorter exposure times and higher strain rates. Various competing factors—including maximum temperature, duration of heat exposure, and changes in speed parameters—contribute to the differing amounts of silicon accumulation and strain rate observed in the welds.
- (2) The distribution and generation of precipitates are not the main factors causing the increased surface hardness in the 56 mm weld compared to the 0-mm and 112-mm welds. Instead, the reduction in ordered texture or increased randomness due to higher plastic deformation, resulting from increased rotational speed, traverse speed, and plunging force, is found to cause the variation in mechanical properties between the weld groups.

- (3) KAM results show an increase in misorientation of the grains and suggest an increase in the density of dislocations due to higher rotational speeds, traverse speeds, and plunging forces. Additional increased misorientations indicate more severe plastic deformation. Although there is less sticking with increased rotational speed, the material that does stick experiences much higher plastic strain. The competing factors of increasingly less ordered texture and higher plastic strain with increased rotational speeds may explain why welds conducted at higher rotational speeds have increased hardness.
- (4) With a constant speed ratio (rotational speed/traverse speed), competing mechanisms keep the grain size relatively consistent, only deviating by a few microns from each weld group as the rotational speed and traverse speed increase.

Having analyzed the specifics of each weld in the battery tray, particularly the top surface, and observed differences in their microstructure and silicon content between the weld and adjacent material, the next section will focus on the residual stress distribution in the battery tray. It will explore straightening processes, such as burnishing and coining, detailing how these methods redistribute stresses and their effects on distortion. Chapter 4 will delve into specifics, considering some of the conclusions made in this section, particularly regarding the constant speed ratio and its effects on grain size, as well as the significant role texture may play in the properties of friction stir welded aluminum. This discussion follows the conclusion of the battery tray analysis, exploring more fundamental aspects of friction stir welding.

3.3 Analyzing Distortion and Stress Effects in Response to Straightening the Battery Tray

The magnitude and shape of the stress profile in FSW are influenced by the heat generation profile, which is asymmetric and determined by tool geometry, rotational speed, traverse speed, and plunging force (Peel et al. [11]; He et al. [12]; De Giorgi et al. [155]; Nie et al. [156]). This asymmetric heat generation profile leads to non-uniform temperature gradients and, ultimately, thermal stresses. Masubuchi et al. [80] define thermal stresses as stress created by non-uniform temperature changes that, once equilibrated, generate residual stress. The non-uniform temperature changes generated during FSW result in residual stress as the weld cools, generating compressive plastic strain as the material expands and contracts due to the heat generated at the source (Richards et al. [7]). Feng et al. [179] explain that the plastic strains that accumulate during the thermal weld

cycles are predominantly compressive and remain in the weldment. As the weldment cools to room temperature, tensile residual stresses of yielding magnitude are retained, typically leaving the base metal outside the weld region in reduced-magnitude compression. The internal tensile and compressive residual stresses deform the joint to comply with strain compatibility. Therefore, the residual stress distributions and the resulting distortion depend on the final state of the plastic strain distributions. Leggatt et al. [180] state that tensile residual stresses are of yield magnitude (i.e., resulting in component distortion) if the free thermal contraction of the heated material is restrained, causing tension that exceeds the yield strength of the material.

Additionally, Masubuchi et al. [80] list the risks of uncontrolled thermal stress, residual stress, and distortion: cracking and mismatching; high tensile residual stresses near the weld causing fatigue damage, stress corrosion cracking, and fracture under certain conditions; distortion and compressive residual stress in the base plate, which can reduce the buckling strength of structural members. Therefore, the residual stresses and distortion in welded components must be controlled. Two methods for potentially controlling residual stress and its corresponding distortion are burnishing and coining straightening techniques applied after the welding operation. Jayaraman et al. [181] describe burnishing as a low plasticity technique that introduces a surface layer of compressive residual stress, which, when used post-FSW, increases the high-cycle fatigue endurance of the aluminum compared to an un-burnished FSW weldment. On the other hand, the coining operation is relatively specific to individual component geometries. This operation requires specialized die tooling and a supportive structure below the component to deform the part and restore its geometry to the design specification. Hence, the coining operation is similar to the post-weld global mechanical tensioning (PWGMT) method described by Altenkirch et al. [92]. PWGMT is a stress leveling technique requiring high tensioning loads that exceed the cold weldment yield strength to elongate the weld material plastically, restoring the component's geometry to the design specification. The plastic deformation caused by the burnishing and coining of FSW components will uniquely impact the residual stresses in the component post-straightening. Consequently, analyzing the residual stress is crucial for optimizing the distortion and service life of any straightened FSW components. However, many studies (Peel et al. [11]; He et al. [12]; Woo et al. [182];) focused on butt joint welding of similar aluminum alloys with no connection to tangible component distortion. Few studies have explored residual stress in lap weld

configurations involving dissimilar aluminum alloys. Even fewer have examined the impact of straightening operations on residual stress in friction stir welded components of this nature.

Therefore, this section aims to understand different stress states formed during these processes. This insight into stress redistribution and relief during each process enables automotive manufacturers to minimize residual stress generation. These results are critical for process optimization to minimize tensile stresses. Residual stresses were determined by employing neutron diffraction to perform several line scans along and through the FSW lap joints in the battery tray before and after applying two different straightening operations (i.e., burnishing and coining).

3.3.1.1 Post Weld Straightening

The utilization of FSW in the manufacturing process of the battery tray results in significant distortion of the primary geometry of the structure (see Figure 71). As a result, the battery tray requires post-weld straightening. The current study examined the residual stress distribution for the burnishing and coining straightening techniques as an effective way of reducing distortion (see Figure 70). In Figure 71, the CMM image shows that the most significant distortion is in the Z direction for the non-straightened tray. More specifically, the center portion of the non-straightened tray experiences between -1.5 to -3 mm of distortion, while the left and right wings experience a +3 mm distortion. Following the burnishing or coining operation, the battery tray geometry was restored to ± 0.75 mm of the pre-weld geometry.

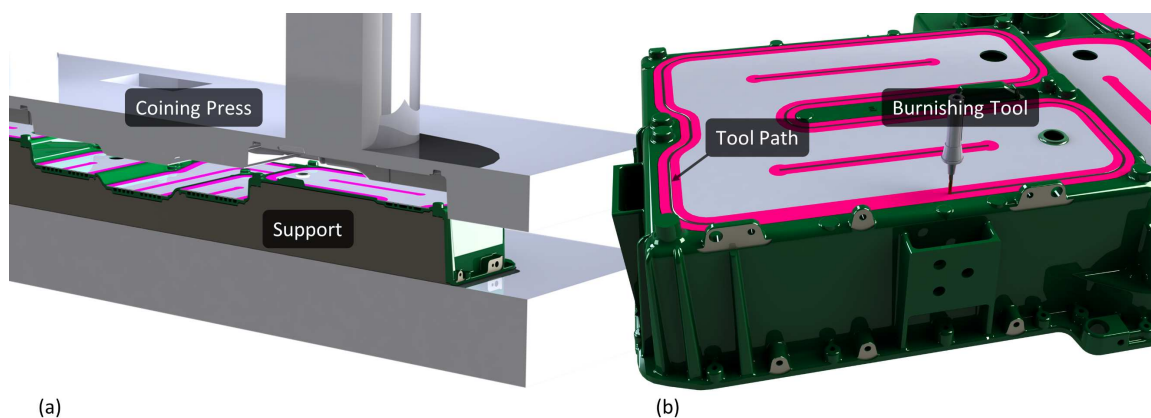


Figure 70: (a) Cross-sectional view of the coining operation straightening via a hydraulic press supported on the inverted side; (b) Burnishing tool straightening via surface layer compression

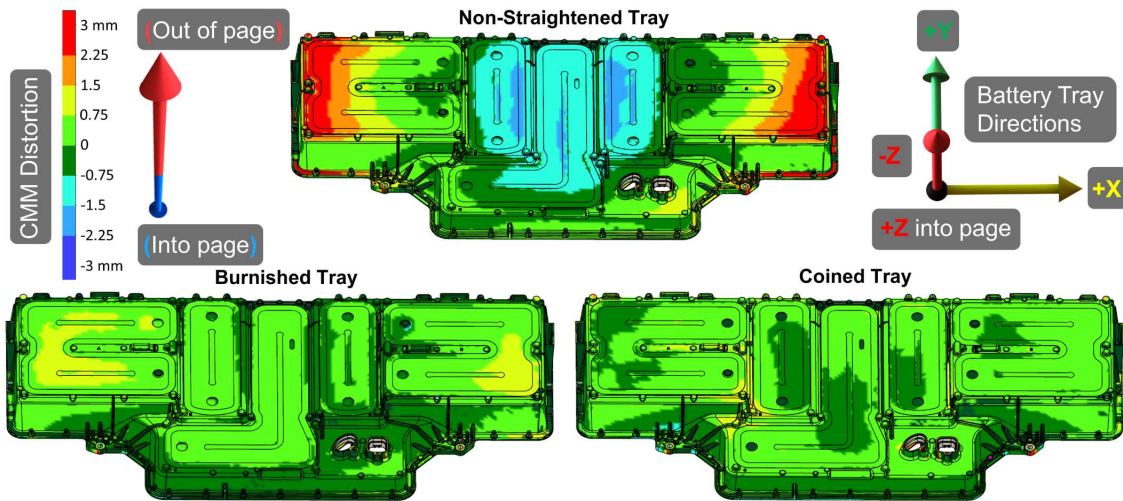


Figure 71: Coordinate machine measurement (CMM) for the non-straightened, burnished, and coined tray (out-of-page distortion shown in red and into-page distortion shown in blue)

3.3.1.2 Neutron Diffraction Scanning Layout Utilized

The neutron diffraction experiments for the battery tray were conducted at the High Flux Isotope Reactor facility at Oak Ridge National Laboratory (ORNL) using the HB-2B High-Intensity Diffractometer for Residual Stress Analysis (HIDRA) instrument. The neutron beam measured strain in three orientations: longitudinal (X), transverse (Y), and normal (Z). The strain and resulting stress were calculated using the peak-shift method of diffraction patterns obtained from the {311} crystallographic planes of the aluminum lattice (Sediako et al. [16]). The peak-shift method requires a stress-free diffraction pattern from a material with the same composition, microstructure, and processing history as the component of interest. Therefore, a comb-style set of stress-free samples were extracted from identical battery trays (see Figure 72), following methods similar to those used by Sediako et al. [16] and Liu et al. [135]. Each stress-free matchstick sample was 20 mm in length and had a cross-sectional area of 3x3 mm². A gauge volume of 2x2x10 mm³ was used for measuring both the stress-free interplanar spacing (d_0) and the stressed interplanar spacing (d) of the battery tray. The scanning region (see Figure 72) was confined to the left wing of the battery tray due to the limitations of the HIDRA instrument, which is restricted to a working area of approximately 400x400 mm². This region consisted of five scan lines: the -81 mm line (blue), 0 mm line (red), +81 mm line (yellow), extended 0 mm line (orange), and the cast line (purple), as shown in Figure 72.

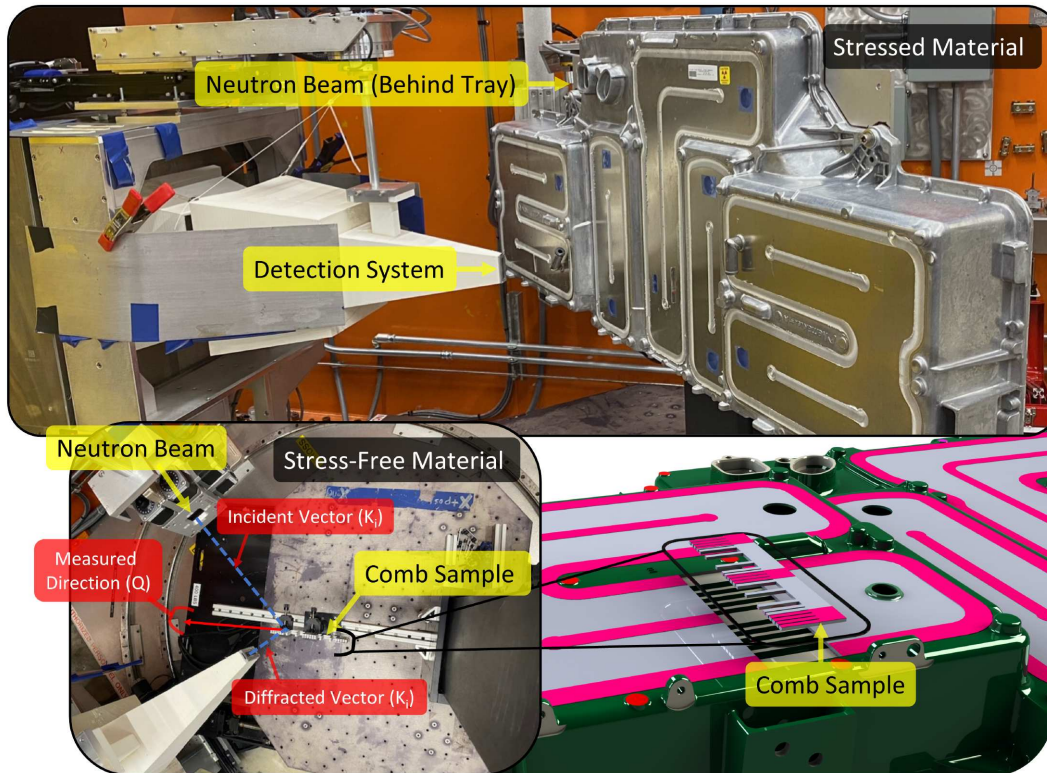


Figure 72: Full stressed component and stress-free comb sample setup on HIDRA instrument

Each dotted grey line in Figure 72 corresponds to a weld line on the battery tray at 0, 56, 112, 155.5, and 213 mm with respect to instrument zero. Figure 72 also depicts a slice removed from the tray to visualize the internal points of the cast line; however, no cuts or sectioning were made on the battery tray scanned to obtain residual stress. The red, blue, yellow, and orange scan lines were measured 2 mm deep into the 6061 plate, while the purple scan line took measurements inside the A365 cast material at varying depths up to 10 mm from the surface. Each scan line was measured on the non-straightened, burnished, and coined battery trays. Following the scanning of all battery trays, the residual stresses were calculated using the method described in the section titled “2.4.3.2 The Peak-Shift Method in the Calculation of Residual Stresses”. The corresponding modulus of elasticity and Poisson's ratio, required for stress calculations, were obtained from tensile tests (see Figure 73b).

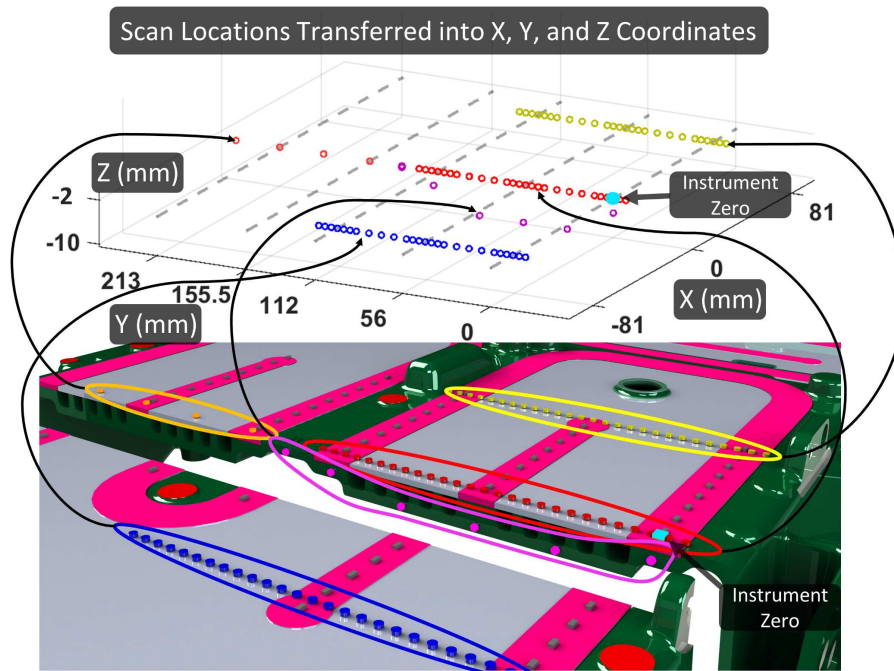


Figure 73: Scanning region for the battery tray that includes several friction stir welds

3.3.1.3 Tensile Sample Preparation

Uniaxial tensile testing was conducted on several tensile samples cut from multiple parts of the battery tray: A365 cast, base 6061 (unwelded region), cross 6061 (samples across the weld), and direct 6061 (samples directly from the welded region) (see Figure 74b). The 6061 samples were cut from all three trays (i.e., non-straightened, burnished, and coined) to check for variability in results due to straightening. Each sample was machined following the principles outlined in ASTM Standard E8/E8M-16a [183]. Strain measurements were obtained using an extensometer, maintaining a constant strain rate of 0.5 mm/mm/min throughout the tensile testing. Following the tensile tests, the ultimate tensile strength (UTS), yield strength (YS) at 0.2% strain, Young's modulus, and elongation were determined from the measured tensile data.

3.3.2 Tensile Properties of Friction Stir Welds

The tensile test results demonstrate that the FSW operation significantly influences the mechanical properties of the Al 6061 material once welded. For instance, the cross-weld samples fractured immediately above the weld during testing (see Figure 74a); therefore, the tensile properties of these tests reflect the weld-affected regions, specifically the TMAZ, rather than the weld itself.

However, irrespective of the fracture location, the modulus of elasticity (E) did not vary considerably between the base, cross, and direct weld-affected 6061 samples (see Figure 74b). The comparable moduli of elasticity between the base, cross, and direct Al 6061 material are attributed to the characteristics of the solid-state welding process; welding mainly affects the microstructure while retaining most of the material's elemental composition, the modulus of elasticity tends to remain insensitive to welding (Pauleau et al. [184]). Consequently, the samples taken from the burnished and coined trays exhibited negligible differences in their moduli of elasticity.

However, the yield strength increases progressively from the base 6061 (unaffected) to the cross 6061 (weld-affected) and the direct 6061 (welded). This observation was consistent across the five samples tested for each condition, with the sample batch for a specific condition being within 5% of each other's elastic modulus and yield strength values. The increasing yield strength from the base, cross, and direct 6061 material is attributed to the worked, refined, and recrystallized microstructure resulting from the friction stir welding process (Lemmen et al. [58]).

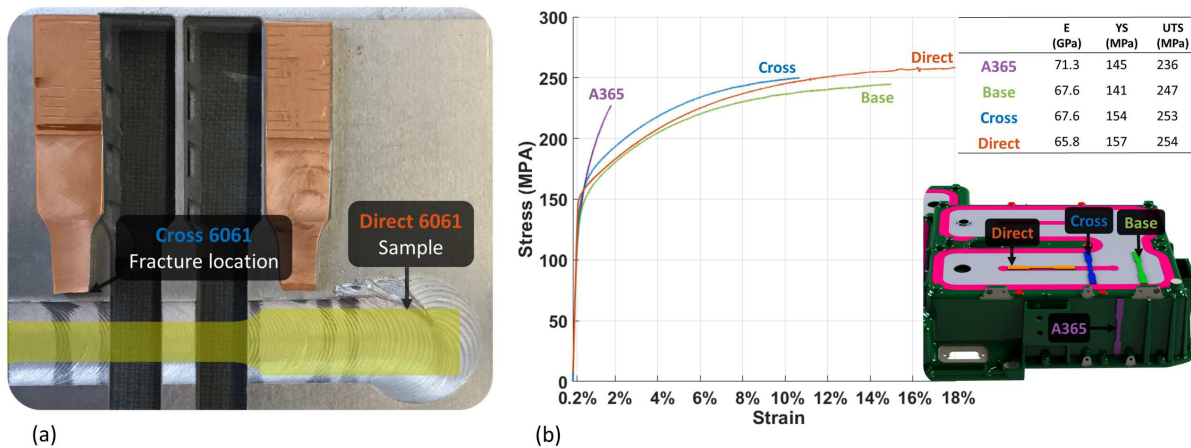


Figure 74: (a) Fracture locations of cross-weld samples and direct-weld sample location; (b) Tensile data of A365, 6061-Base, 6061-Cross, and 6061-Direct material

3.3.3 Residual Stress in As-Welded Non-Straightened Tray

Figure 75a indicates the location of three of the scan lines for each battery tray, as shown by the blue line at -81 mm, the red line at 0 mm, and the yellow line at 81 mm. The grey dotted lines correspond to the weld location on the model at 0, 56, and 112 mm from the component reference in Figure 75a. The 3D stress profile is shown in Figure 75b, and the corresponding projected 2D profile is shown in Figure 75c. This method of presenting the data will be used throughout this section. Moreover, this section will present data for the longitudinal-X, transverse-Y, and normal-

Z directions (see Figure 75a for directions), corresponding to residual stress measured in the X-direction, Y-direction, and Z-direction of each battery tray.

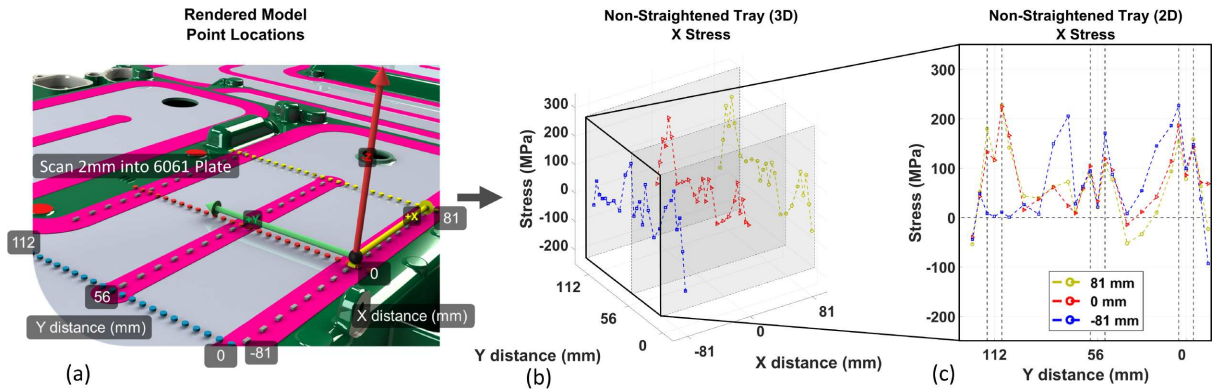


Figure 75: (a) Visualization of scan point locations; (b) Three-dimensional view of X-direction stress data; (c) Two-dimensional view of X-direction stress data

An evident change in the stress profile is present when comparing the welds with much higher residual stress within the dotted vertical lines in Figure 75c to the base Al 6061 material with much lower residual stress, with some points near zero; this is present for all lines except at the -81 mm position (blue line in Figure 75) between the 112 and 56 mm weld position, this shows an increase of stress outside the weld region between the two vertical lines containing the 56 mm weld, which tapers off to zero near the 112 mm line. This trend continues into the Y and Z-direction (see Figure 76), with zero stress magnitudes for the X and Z direction and compressive stresses in the Y direction for the 112 mm position, followed by a rapid increase in stress as the 56 mm weld position is approached. This is ultimately due to the design of the weld layout, as the weld positioning is different near the blue line compared to the red and yellow scan lines, as they have a weld at the 112 mm position, shown in Figure 76b, leading to the maximum tensile stress at the outlined profile in Figure 76a. This weld path design leads to considerable tensile stress in the valley between the 112 mm and 56 mm weld, possibly becoming the most strength-compromised region. This demonstrates that the weld path design is crucial for the non-straightened battery tray. For the pre-straightened battery tray, the most extensive measured residual stress is in the Y direction, and it is not even in the weld zone. Notably, the region highlighted by the black circle in Figure 77B for the blue line is surrounded by multiple welds, although it does not encounter a weld itself. Although these scans are done in straight lines horizontally across the battery tray, the competing

residual stress fields between the bottom of the plate, the center of the plate, and the welds near the 112 mm location or left of the center likely contribute to this unusual increase in residual stress.

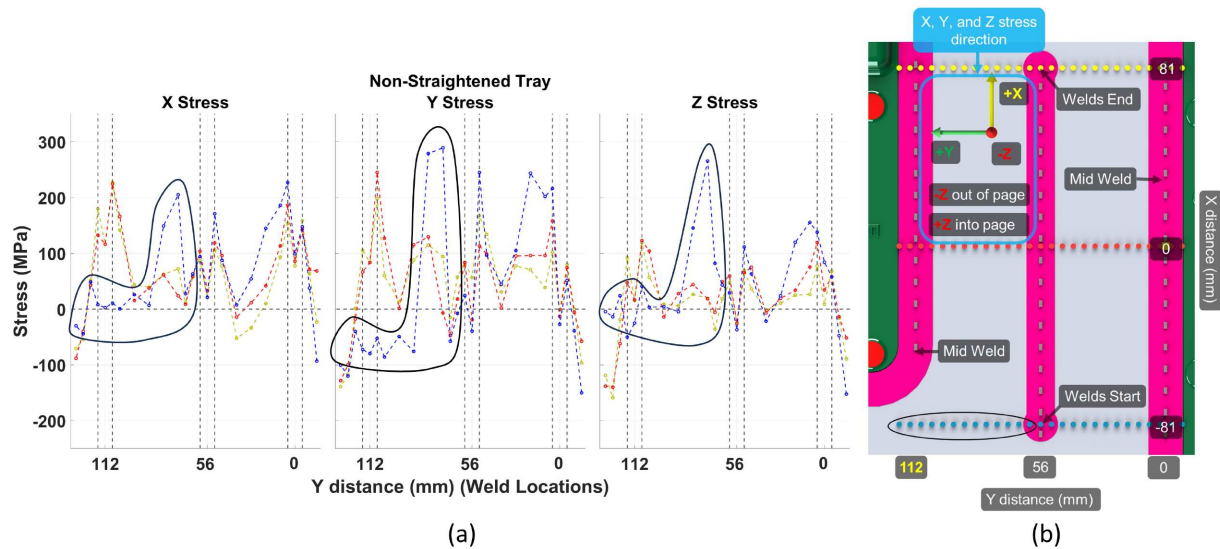


Figure 76: (a) X, Y, and Z stress directions (non-straightened tray); (b) Top view of scanned location highlighting the start of the blue scanning line finishing absent of the weld (copy of Figure 58 for reading convenience)

Traditionally, the Z stress, or normal stresses, are assumed to be zero through the plane stress condition. However, in this specific region highlighted by the black circle in Figure 77A, the normal stresses are even larger than the longitudinal stresses, whereas the stresses within the actual weld regions are comparatively much smaller. This suggests that the observed residual stress may not be strictly due to the weld itself but could be influenced by the plate's reaction to the surrounding casting material or the fact that the casting material was not stress-free before welding.

This is an important consideration for industrial components that undergo multiple processing steps beforehand. High-pressure die-cast components may not necessarily be stress-free before welding. In fact, a few studies (Kianfar et al. [185]; Hofer et al. [186]) indicate that high-pressure die casting components do generate some tensile stresses, which may depend on the thickness of the cast. To investigate the potential residual stresses within the cast section of the battery tray component, the neutron diffraction experiment included two more scan lines. The first is the extended orange line that advances into the adjacent 6061 plate, depicted on the rendered model in Figure 77. The second line measured stress in the A365 cast material, shown by the purple dots on the green portion of the rendered model in Figure 77. Figure 77b-c translates the 3D view of

the X-stress into the second dimension. In the foreground, the scan lines from the previous discussion (blue, yellow, and red plate scans) are included in high transparency to indicate the relative difference between the cast scan and the plate scan. The scan of the cast component had limited points; nevertheless, the purple A365-casting line follows a similar residual stress profile in both shape and magnitude, as compared to the 6061-red line (see Figure 77). The Y- and Z-direction stress (see Figure 78) follow the same pattern but are marginally higher in magnitudes between the welds.

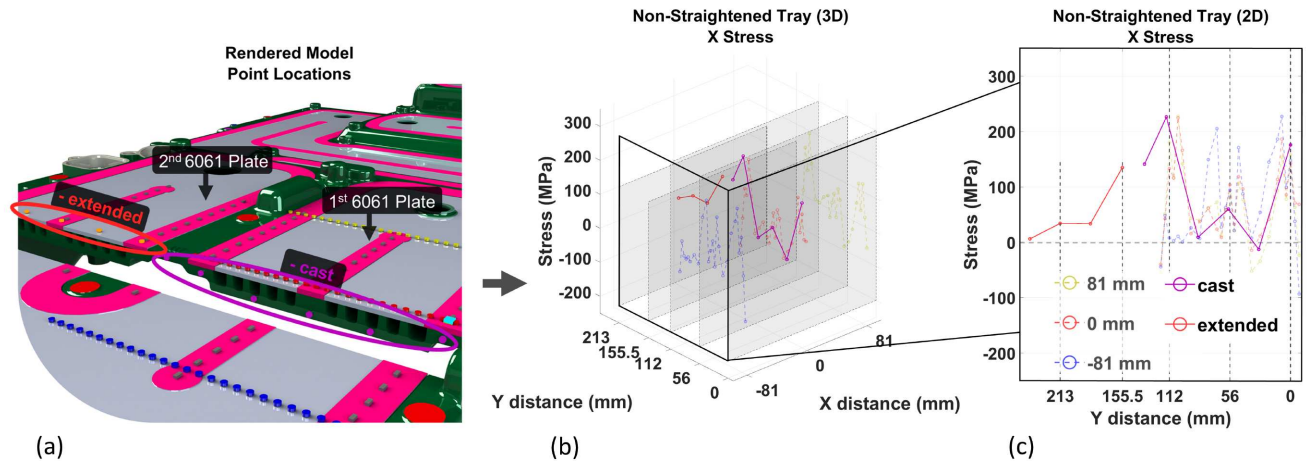


Figure 77: (a) Visualization of scan point locations; (b) Three-dimensional view of X-direction stress data; (c) Two-dimensional view of X-direction stress data

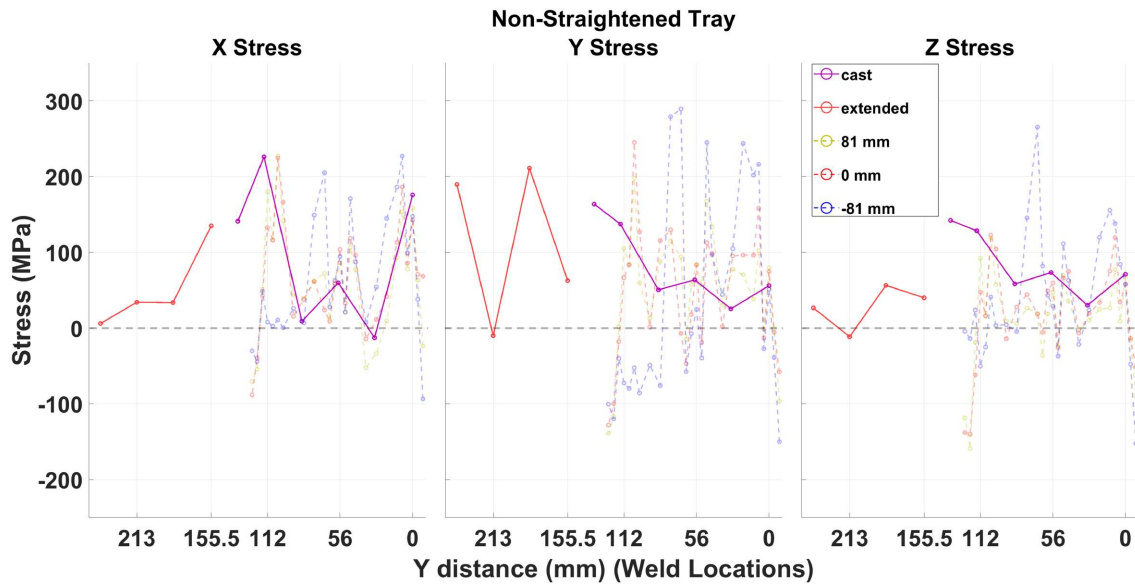


Figure 78: X, Y, and Z direction of the cast and extended line scan

This observation is particularly intriguing as it appears to result from multiple factors. When examining the X- or longitudinal stresses, the profile closely resembles that of the FSW process (see Figure 78), suggesting that the thermal cycle or stresses imparted by FSW may be influencing these measurements. However, when observing the Y-traverse and Z-normal directions, the stresses consistently remain above zero across all points scanned without the extremely pronounced dip between the welds. This suggests that the cast component itself may have had residual stresses before welding occurred. It is plausible that the Y and Z stresses dominate during the rapid cooling process of the high-pressure die-cast material. Stresses transverse and normal to the component may inherently be larger compared to those in the longitudinal direction, contributing to the measurement differences between the directions within the cast (see Figure 78). Although it is challenging to pinpoint the exact origins of these stresses, as the cast component was not scanned separately and prior to FSW, it is difficult to distinguish between stresses caused by the friction stir welding process and those pre-existing due to the high-pressure die-casting process within this region. Nonetheless, it is evident that in-plane stresses, or Z stresses, should not be assumed to be zero when dealing with complex cast components. The friction stir weld joint may inherit stresses from previous processes, impacting the overall stress state upon welding or joining. This phenomenon occurs because the residual stresses present in the cast component before welding can interact with the thermal and mechanical stresses induced during the welding process. This interaction can lead to a redistribution or magnification of existing stresses, resulting in a complex residual stress profile in the welded component (Itoh et al. [187]; Wu et al. [188]).

3.3.4 Residual Stress after Burnishing and Coining

Yield strength measures a material's elastic limit, beyond which it undergoes plastic deformation. In contrast, residual stress represents the stress that remains in a material after deformation, whether elastic or plastic. Interestingly, residual stress can sometimes exceed the yield strength without causing further deformation, as it is often localized and confined within the bulk of the material, which is restrained from all sides. This condition may indicate that the material has undergone significant plastic deformation, with the residual stress being a consequence of that process. It is crucial to understand that residual stress measures the internal stress within the material and is not inherently the stress that caused the deformation. Residual stress can either be a result of deformation or a contributing factor to deformation (distortion). In cases of distortion,

areas with high residual stress may contribute to the overall distortion, but the relationship is not strictly cause-and-effect. Other factors, such as material properties, geometric constraints, and loading conditions, also play significant roles in determining the extent of distortion. Residual stress is a complex phenomenon influenced by various factors, including thermal history, deformation history, and the mechanical properties of the material. Therefore, while this discussion explores the connections between residual stress and distortion, establishing a direct correlation between residual stress exceeding the yield strength and resulting in plastic deformation and distortion may not be straightforward. Nonetheless, it remains a topic worth comparing and examining.

For example, the non-straightened tray exhibits significant distortion (see Figure 71), with the maximum tensile stress in the X-direction exceeding the measured yield strength of the material (see Figure 74 for yield stress). This excess stress possibly contributes to the distortion around the tray or indicates significant plastic deformation. In contrast, after the straightening operations, many of the maximum stress measurements are at or below the material's yield strength, as shown in Figure 79.

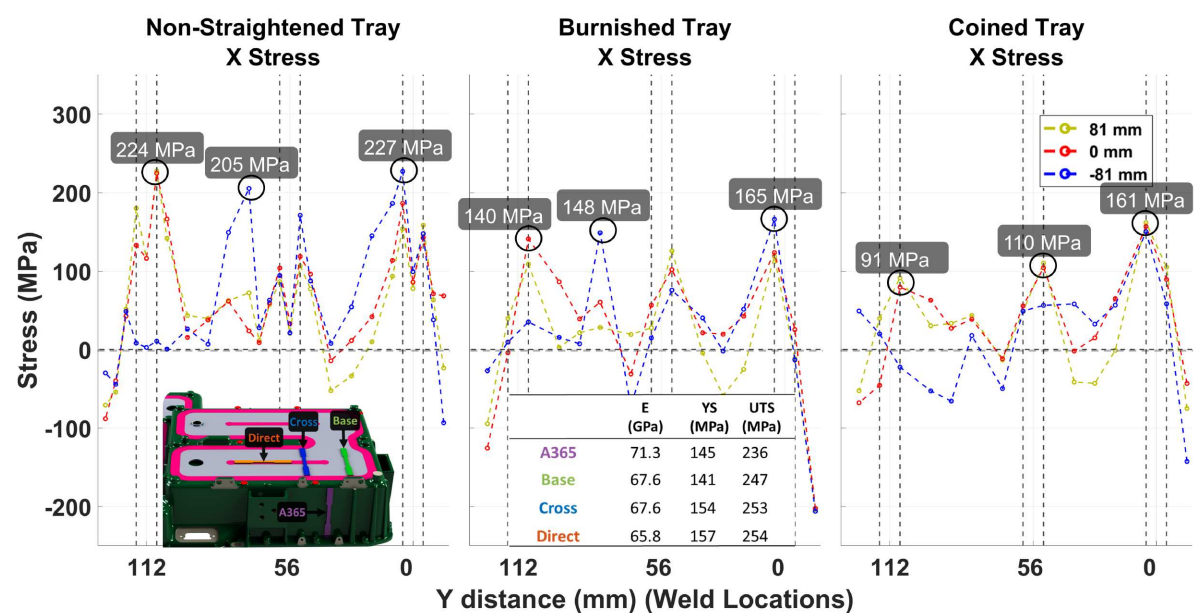


Figure 79: Comparing stress measurements to yield strength after coining and burnishing

Figure 80 projects the top surface of the 3D stress data into a 2D surface map. The 2D top view, shown in Figure 80b, displays the residual stress using a color gradient from blue to red. For

instance, on the right side of Figure 80b, a color bar indicates stress in MPa, where blue gradients denote tension found directly on or near the welds. Conversely, the base metal between the welds includes yellow to red gradients, indicating lower tension or, in some locations, compression. If the color map contains hard-blue gradients (approximately 200-300 MPa), the residual stress exceeds the tensile yield strength of the Al 6061 plate material.

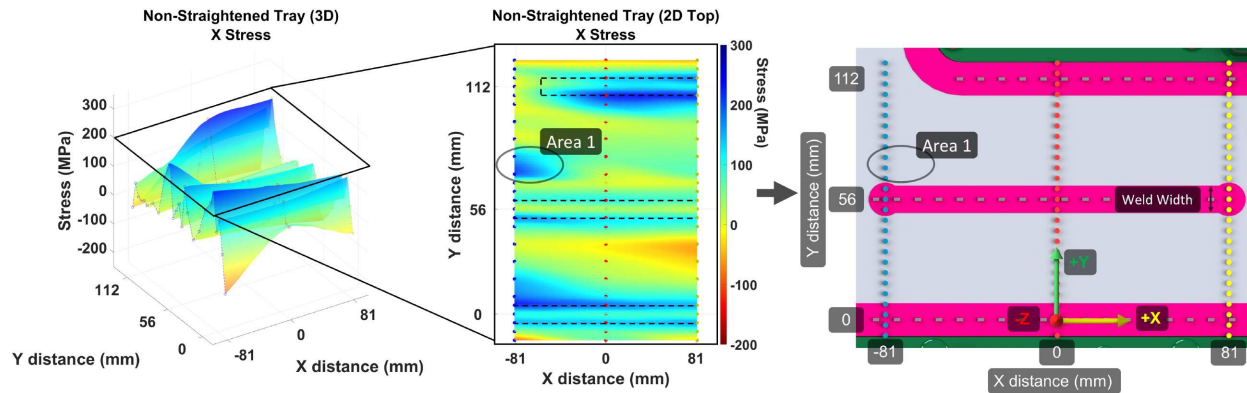


Figure 80: (a) Three-dimensional view of X-direction surface data; (b) Two-dimensional view of X-direction surface data; (c) Visualization of scan point locations

The heat maps in Figure 81 compare the effects of burnishing and coining operations on residual stress. In "Area 1" on the blue scan line in Figure 81, where this portion of the blue line is not on a weld line (see Figure 76), the burnishing operation reduces the maximum tensile stress compared to the non-straightened battery tray. In contrast, the coining operation inverts the color gradient, indicating that the tensile stresses have been converted into compressive residual stresses in most locations. In "Area 2," shown in Figure 81 and adjacent to the burnishing tool's indent path, the effect of the burnishing operation on the stress map is evident in the red gradients, signifying substantial compressive stresses. These compressive stresses appear to be beneficial in reducing distortion along the battery tray (see Figure 71 for the distortion map). To facilitate efficient comparisons of areas with stress above the material's yield strength among the non-straightened, burnished, and coined stress maps, additional data processing was conducted. This processing highlights the amount of residual stress exceeding the material's yield strength, referred to as the "yield area," as displayed in Figure 82.

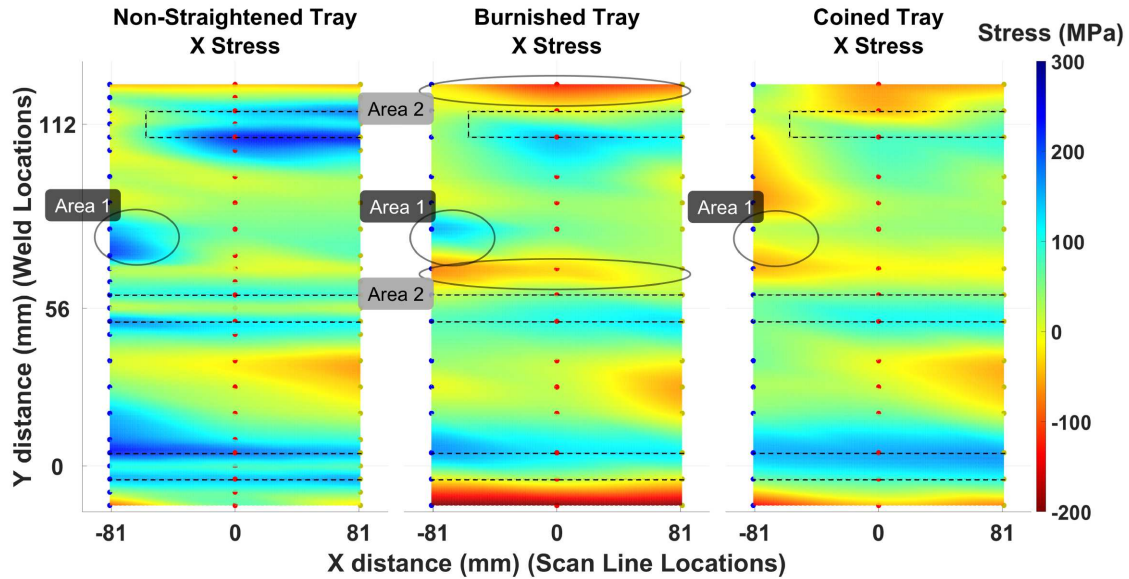


Figure 81: Comparing (X-direction) non-straightened to burnished and coined stress map

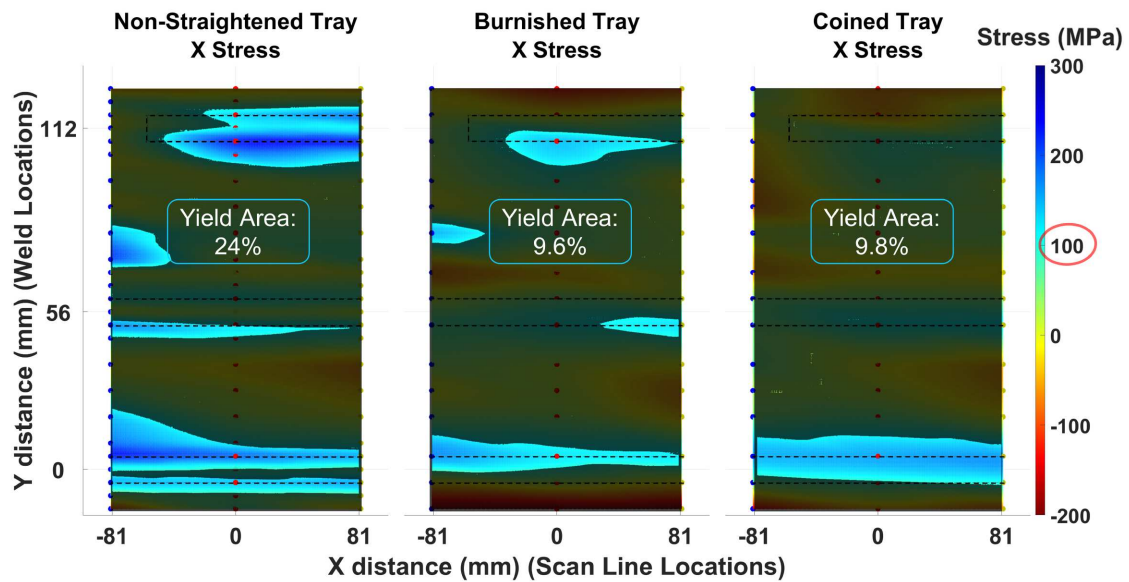


Figure 82: The total yield area of the material is the addition of all the blue areas (readings over 100 MPa) on the surface map

In the non-straightened tray, the total yield area in the X-stress direction is 24%, closely related to the position of the welds on the battery tray (see Figure 82). After the burnishing operation, the yield area is reduced to 9.6%. Similarly, the coining operation reduces the yield area to 9.8% in the X-stress direction. The difference between the straightening operations lies in the locations where they reduce the yield areas. The burnishing operation reduces the yield area relatively evenly across each weld. In contrast, the coining operation is more effective in regions distant from the

casting edge, likely due to the edges being in direct contact with the mounting table underneath the battery tray. In comparison, the middle of the tray, supported by a die, has some room to bend (see Figure 70).

Next, considering the Y-direction of stress in the surface map shown in Figure 83, the coining operation increased the yield area from 23% to 46%, covering a significant portion of the coined tray. This entire region is above the yield strength, suggesting that the operation exceeds the cold weldment yield strength, thereby plastically elongating the battery tray material and resulting in elevated residual stress. Furthermore, since the non-straightened tray is concave in geometry (see Figure 84b) due to thermal contraction after welding, the coining operation acts against the tray's concavity. The black arrows in Figure 84b indicate the direction of the applied force during the coining operation. As the tray is pressed in the direction of the black arrows, the 6061 plates and upper part of the A365 are pulled and plastically deformed across the battery tray, resulting in high tension (see Figure 83, Coined Tray).

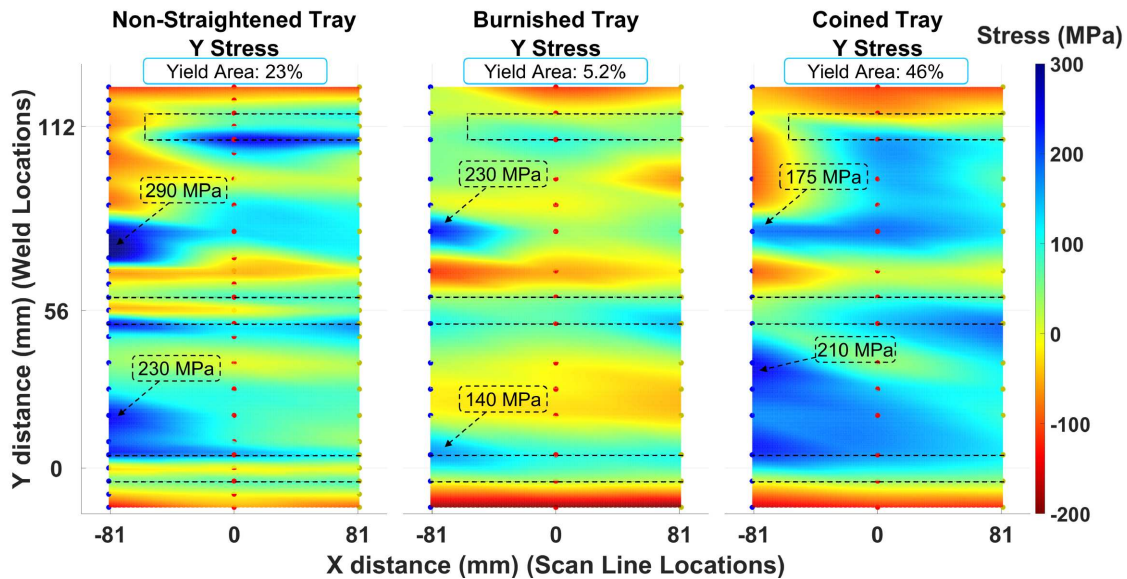


Figure 83: Comparing (Y-direction) non-straightened to burnished and coined stress map

The lower part of the component is subjected to compression rather than tension, though this portion of the battery was not measured in this section. The coining operation redistributes the highest tensile residual stresses between the welds, reducing the maximum tensile stresses while increasing the areas of high tensile residual stress in the component. Although distortion has been addressed, the extensive areas of tensile residual stress may contribute to accelerated fatigue

damage and stress corrosion cracking, potentially limiting the component's service life after the coining operation. Conversely, the burnishing operation reduces the yield area considerably, from 23% to 5.2%. Specifically, near the top left corners of the surface maps, the non-straightened tray exhibits significant stress variance from compression to high tension exceeding the yield strength of the material near the 112 mm weld (see Figure 83). The burnishing operation reduces the stress variance in this location to minimal compression or zero stress. In contrast, for the same location, the coining operation has little effect and retains the high variance between significant compressive stresses and stresses exceeding the yield strength of the material, similar to the battery tray, before straightening.

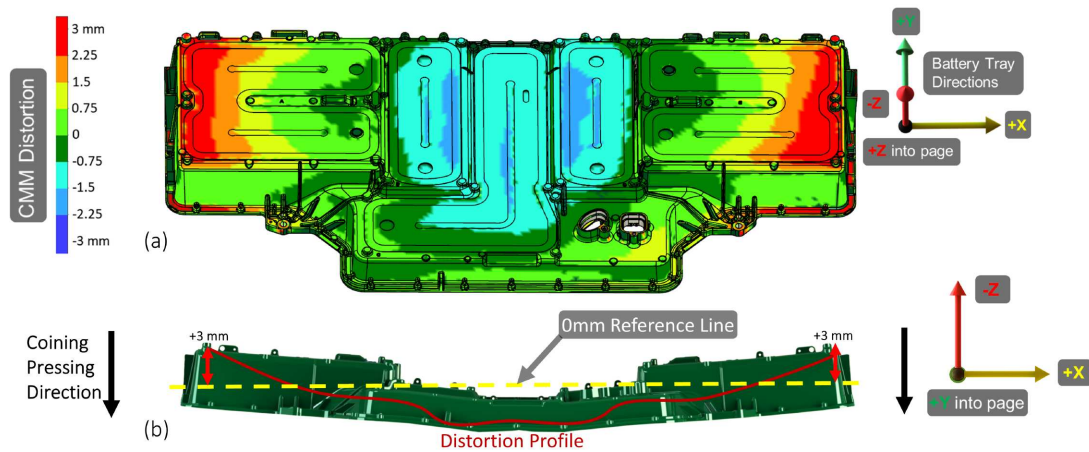


Figure 84: (a) non-straightened tray CMM measured distortion; (b) bending effect on battery tray (black arrows indicating the direction of pressing in coining operation)

Considering the Z-direction of stresses in the surface map shown in Figure 86, the coining operation slightly reduces the yield area from 6.1% to 4%, indicating a constant low-level residual stress in the normal direction. On the other hand, the burnishing operation increased the yield area from 6.1% to 16.3%, suggesting that this process introduced more locations where the residual stresses exceeded the yield strength of the Al 6061 material. When burnishing is applied, the surface layer of the material is plastically deformed, and the material underneath is compressed. This compression induces a state of compressive residual stress on the surface and subsurface layers. However, due to the principle of stress equilibrium, the introduction of compressive stress in one direction may be balanced by tensile stress in the perpendicular direction. This might explain why more tensile stress can be observed in the normal (Z) direction for the burnished map. It is also notable that this increase in yield area is not directly on the welds but in between the welds,

in the valleys. There is still significant compression measured near or on the welds, but the areas in between react with higher tension, thus increasing the yield area. This phenomenon wasn't observed in other directions, which may suggest that the redistribution or principles of balancing stresses are beneficial for the X and Y directions (longitudinal and transverse stresses) but come at the cost of increasing tensile stresses in the normal direction.

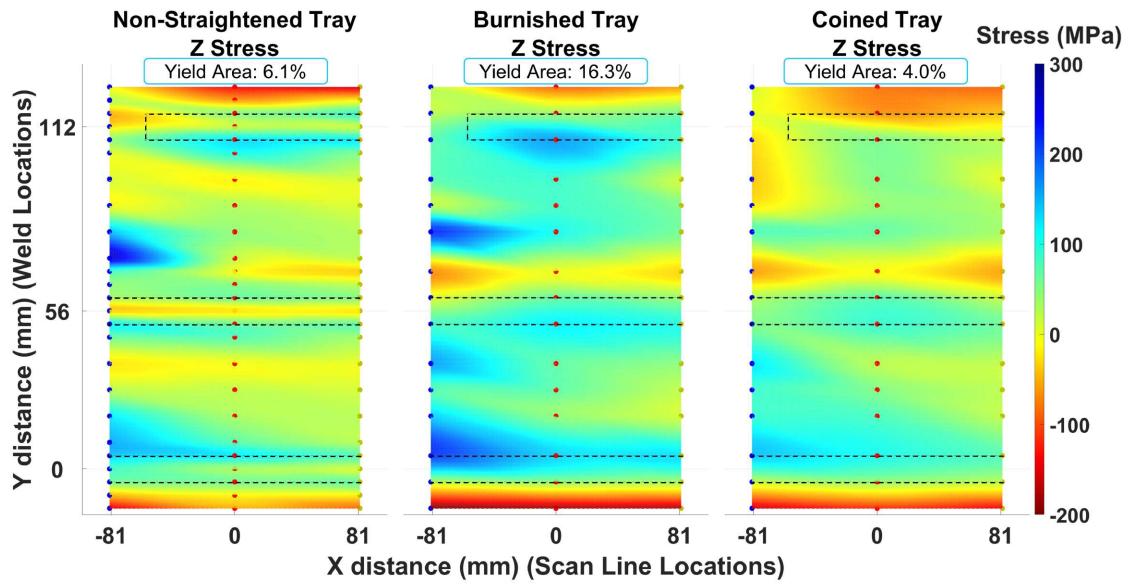


Figure 85: Comparing (Z-direction) non-straightened to burnished and coined stress map

The residual stress maps shown from Figure 81 to Figure 85 represent the stresses locked within the material's bulk. The observed stresses that exceed the yield strength contribute to the initial deformation, after which the residual stresses remain in the component. Although not apparent in the short term, these residual stresses can contribute to long-term distortion and potential cracking, ultimately leading to component failure. However, the distortion that develops over the long term (i.e., over a year) due to the release of residual stress is typically insignificant, on the order of microns (Kianfar et al. [189]; Aval et al. [157]). Part of the stress imposed by the FSW operation is released during the initial plastic deformation, causing the concave curve of the battery tray (see Figure 71). The remaining stress (i.e., residual stress) can be measured in the non-straightened tray (Figure 81 to Figure 85). The burnishing operation helps release the residual stress via plastic deformation. When burnishing is applied, the surface layer of the material undergoes plastic deformation, which redistributes and reduces the internal residual stresses, particularly along the weldments. This process leaves much-reduced residual stress in the X and Y directions, while the

Z-direction (normal direction) compensates by balancing the stress equilibrium. The residual stresses in the X and Y directions significantly determine the distortion occurring in the battery tray, as these directions influence the concave geometry of the non-straightened battery tray. This is true because residual stresses in the X and Y directions can cause bending or warping in these planes, which contributes to the overall concave shape. Therefore, the relatively higher residual stresses in the Z-direction should not considerably affect the overall distortion. This is likely the case, as Z-direction stresses are more likely to cause localized thickness changes rather than large-scale shape changes. In contrast, the coining operation strictly deforms the casting geometry, causing plastic deformation that releases stresses in the X direction but adds residual stress in the Y direction, while the Z direction seems unaffected due to the fact that bending is caused by X and Y residual stresses. Instead of releasing residual stress to bring the geometry into the pre-weld dimensions, the coining process presses and deforms the battery tray into those dimensions. Both processes achieve good dimensional accuracy (see Figure 76); however, the targets by which they achieve this are completely different.

Lastly, the effects and differences between the straightening operations are clearly evident when examining the measured residual stresses within the cast component (see Figure 86). The coining operation results in more significant longitudinal (X-direction) stresses compared to the non-straightened tray. In contrast, the burnishing operation significantly reduces those stresses in the same locations.

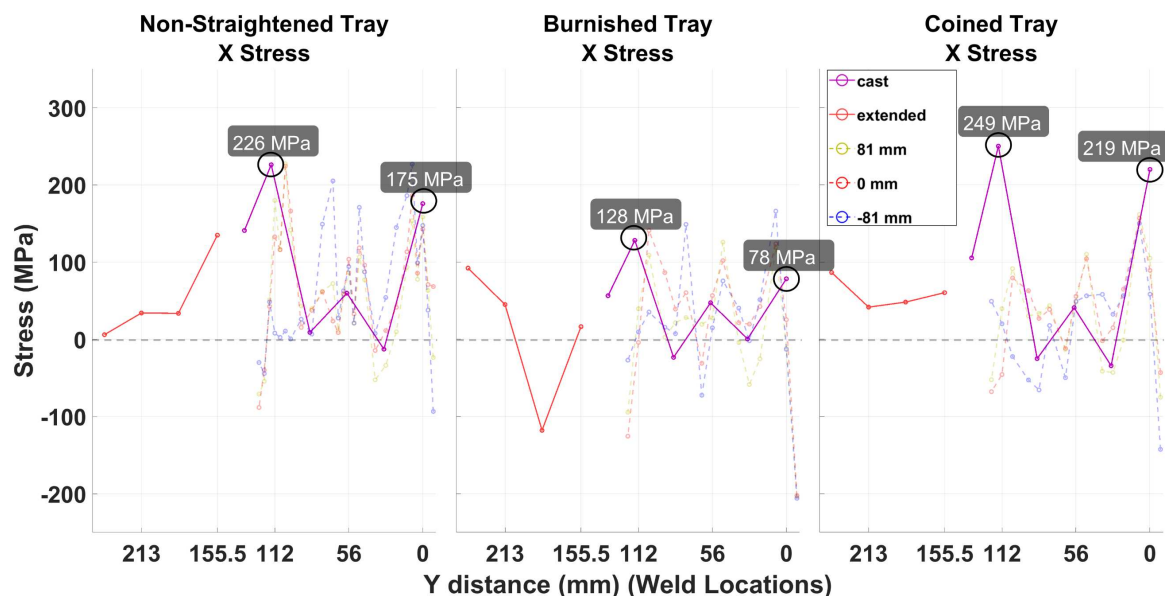


Figure 86: All 5 Scan lines comparing straightening operations (X-direction)

3.3.5 Section Conclusions

This section analyzed the friction stir welding process, which caused significant battery tray distortion, and the post-welding straightening operations used to restore the geometry to its original state. While both operations effectively reversed the original distortion, their impact on residual stress was previously unknown. Therefore, a comprehensive residual stress analysis was performed using neutron diffraction on the original distorted tray, the burnished tray, and the coined tray. The following conclusions can be drawn from this section:

- (1) After the FSW process, the yield area (i.e., the area containing residual stress above the material yield strength) accounted for 24, 9.6, and 9.8% of the scanned area for the X-direction, Y-direction, and Z-direction of stresses, respectively.
- (2) The FSW process imparts significant plastic deformation, leading to large residual stresses shown in the yield area, causing distortions of the battery tray upwards of ± 3 mm at the wings and center of the battery tray, as compared to the pre-weld geometry.
- (3) Following the burnishing operation, the measured residual stress in the scanned region revealed evidence of surface layer compression induced by the burnishing technique. In addition, the burnishing operation reduced the yield area by 14 and 18% in the X-direction and Y-direction, respectively. In contrast, the Z-direction stresses increased the yield area by 10% after the burnishing operation. Nonetheless, the burnishing operation restored 93% of the battery tray's surface area to within ± 0.75 mm of the pre-weld geometry.
- (4) Following the coining operation, the yield area is reduced by 14% in the X-direction of stresses. The yield area in the Z-direction stayed nearly constant, only decreasing by 2%. However, the battery tray's significant distortion profile was reduced by the operation, which required considerable plastic deformation to restore the pre-weld geometry, increasing the yield area by 23% after straightening in the Y-direction of stress. Nevertheless, the coining operation restored 98% of the battery tray's surface area to within ± 0.75 mm of the pre-weld geometry.

This section provided an improved understanding of the relationship between the friction stir welding operation, straightening operations, and the corresponding alleviation or generation of residual stresses. These insights can be used to further optimize the friction stir welding process,

thereby minimizing the generation of residual stresses in complex aluminum components. This optimization will help reduce component distortion and improve dimensional accuracy in the final product. However, some complex residual stress profiles are difficult to understand due to the potential initial stress state of the high-pressure die-cast component. Additionally, the effects of multiple welds in close proximity are not clearly understood. Many residual stress profiles for the higher welding speed center weld or non-peripheral weld show considerably lower residual stresses. The current theory suggests that because the center or non-peripheral weld was conducted first, followed by the peripheral weld, the plastic deformation and related heat from the subsequent peripheral weld redistributed the center weld's stresses to a lower state, potentially by releasing it through further deformation. Individually, increased welding speed typically results in higher residual stresses, but the presence of multiple welds seems to alter this outcome. The generation and subsequent release of residual stress, leading to plastic deformation or distortion, are likely influenced by the application of multiple welds rather than single welds; this is a key focus in Chapter 4. Chapter 4 begins with a single weld in a controlled plate geometry to reduce complexity. By systematically increasing the welding parameters independent of other welds, it becomes possible to pinpoint the exact effects of residual stress. Subsequently, using consistent welding parameters while adding multiple welds on the same plate geometry allows for further analysis of whether the proximity of multiple welds is causing the observed distortion.

Chapter 4: Stress, Texture, Distortion, and Simulations on the Friction Stir Weld Utilized on Controlled Plate Geometry

This chapter encompasses the research carried out on the in-house cast plates, specifically A365 plates, with subsequent 6061 plates welded on top using friction stir welding. The experiments executed in this chapter are divided into sections that include an initial examination of the single-welded plates and an in-depth discussion of their findings, followed by an exploration of the impact of multi-welded plates and the resulting outcomes.

4.1 Residual Stress Characteristics in Friction Stir Welding: An In-depth Temperature, Texture, and Neutron Diffraction Analysis of Speed Parameters

This section investigates the influence of welding parameters on single-welded plates of aluminum alloys 6061 and A365. The study employs neutron diffraction, EBSD, and SPH to analyze texture, torque, temperature, and residual stress. Different welding speeds, maintaining a constant speed ratio, were tested to observe their effects. Key findings include that higher welding speeds reduce the required torque, increase maximum temperatures, and influence residual stress distributions, especially in the longitudinal direction. Texture intensity decreases with increased welding speeds, potentially affecting the yield strength and subsequent residual stress profiles in the welded material.

4.2 Influence of Multiple Welds on Residual Stress and Distortion in Friction Stir Welding: A Neutron Diffraction Study

This section investigates the influence of welding sequence and proximity on residual stress generation in FSW aluminum plates. It focuses on the effects of different sequences of welds and the impact of thermal gradients and residual stress interactions. The study uses various plates with different welding configurations, including single central welds, peripheral welds, and combinations of both, to analyze how these factors affect residual stress and distortion. Key findings highlight that distortion primarily appeared in multi-welded components, with similar high distortion profiles observed when the sequence of welds was close in time. However, extending the duration between welds and the resulting thermal gradients substantially influenced the residual stress profiles, leading to different magnitudes and patterns of distortion compared to other tested plates. The study emphasizes the importance of managing thermal dynamics and weld

sequencing to optimize weld quality and minimize residual stress and distortion in multi-welded components.

4.1 Residual Stress Characteristics in Friction Stir Welding: An In-depth Temperature, Texture, and Neutron Diffraction Analysis of Speed Parameters

Understanding the influence of the structural type on residual stresses is essential for accurate property measurements in structural components. For example, in Chapter 3, it was found that residual stresses in unique battery tray structures differ from those observed in small flat test coupons due to geometry considerations of both the component and the weld itself. Moreover, understanding the local temperature environment is essential for better comprehension of stress evolution. To accurately determine these environments, combining experimental measurements with simulations provides the most representative understanding. This approach captures conditions directly interacting with the tool and at some distance away. Smoothed Particle Hydrodynamics (SPH) models are often preferred over Finite Element Method (FEM) and other models for simulating the FSW process due to several reasons. One such reason is FSW involves topology changes and large deformations, which pose difficulties for the commonly used FEM due to unclear mesh connectivity.

In contrast, SPH, with its mesh-free nature, provides a suitable framework for the simulation of FSW. For example, Fraser et al. [35] utilize SPHriction-3D, a mesh-free, fully coupled thermo-mechanical approach designed for highly non-linear, dynamic multiphysics simulations, specifically used to model the large deformations in FSW accurately. Additionally, to accurately define the effect of elevated temperature on aluminum strain rate sensitivity, a specific flow stress model was developed to account for strain hardening, thermal softening, and strain rate effects (Fraser et al. [190]). This approach has also seen broader applications in its versatility in solid-state additive manufacturing processes and additive friction stir deposition (Stubblefield et al. [191]). Another study highlighted its capability to precisely capture the influence of temperature and strain rate on the mechanics of additive friction stir deposition (Stubblefield et al. [192]). These implementations underscore the model's robustness and adaptability across varying scenarios.

Consequently, it is critical to extend the knowledge of residual stresses beyond just test coupons to include full-scale structural elements and accurate temperature simulations. However, completing comparative studies between many parameters becomes difficult with full-scale components. For the present section, an intermediate solution was found by creating a plate weld setup comparable to a structural industry element with a 175 mm long weld. The goal of this section is to examine the interplay between welding parameters and multiple performance outcomes, such as texture, torque, temperature, and residual stress. This investigation aims to fill this gap by employing a holistic approach that integrates various measurements and analyses to understand how welding parameters influence the characteristics of a welded joint between aluminum wrought (6061) and cast (A365) alloys. Using the widely studied 6061 and A365 Al system, the effect of four specific welding parameters with increasing rotational and traverse speed at a consistent speed ratio of 1.57 (rev/mm), including the spindle torque data, temperature simulations, residual stress, and texture in a wrought-to-cast lap FSW has been demonstrated and characterized using neutron diffraction, EBSD, and SPH offering a distinct perspective compared to existing research with the explicit intention of understanding how all these items relate to potentially better control the residual stress response to reduce its side effects and increases its benefits.

4.1.1.1 Weld Layout and Quality Assurance of Single Welded Plates

Friction stir welding (FSW) has been extensively employed to join diverse materials, with a particular emphasis on aluminum alloys, given their widespread industrial applications. The present section utilized A365 aluminum castings as the substrate material, onto which 6061 aluminum alloys were friction-welded (see Table 1). Specifically, the dimensions for the 6061 alloy were $400\text{ (l)} \times 200\text{ (w)} \times 2\text{ (t)}\text{ mm}^3$, while the A365 alloy possessed dimensions of $400\text{ (l)} \times 200\text{ (w)} \times 7\text{ (t)}\text{ mm}^3$ (see Figure 87). Regarding the welding process parameters, the study examined four distinct welding configurations listed in Table 3 and displayed in Figure 87. While speed parameters (rotational and traverse speed) vary, certain aspects remain consistent. The speed ratio (rotational speed/traverse Speed) was maintained at 1.57 across all welding operations. The axial downward force, an important parameter affecting the advent of weld defects, was determined based on the weld quality.

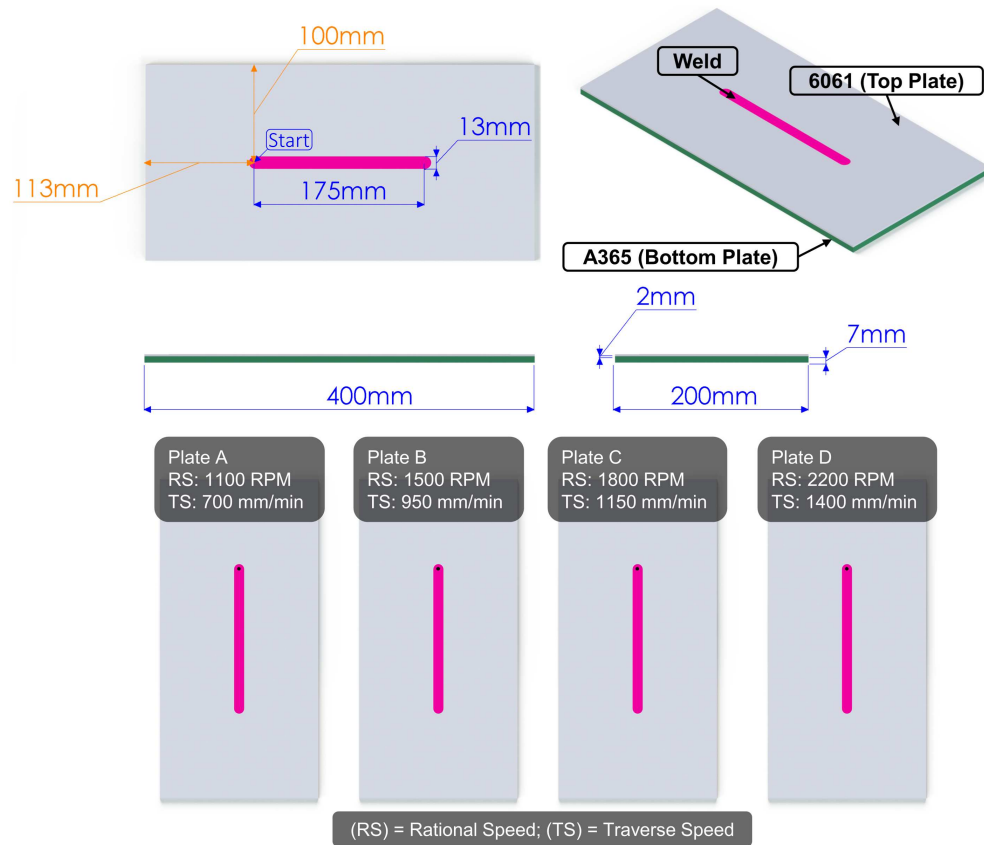


Figure 87: Plate dimensions and weld parameters for single welded friction stir welding study

Table 3: FSW processing conditions (single welded plates)

Weld Plate	Dwell Time (s)	Traverse Speed (mm/min)	Rotational Speed (RPM)	Plunge Force (kN)	Plunge Feed (mm/min)	Withdraw Feed (mm/min)
Plate A	1.2	700	1100	7.3	120	250
Plate B	1.2	950	1500	7.3	120	250
Plate C	1.2	1150	1800	7.3	120	250
Plate D	1.2	1400	2200	7.3	120	250

To assess the quality of the weld, an ultrasonic analysis employing a 30MHz focalized probe and 0.1mm increment steps was utilized during the developmental phase to determine the optimal downward force. The optimal axial force was determined through test welds that were completed on development plates with gradually increasing axial force until defects ceased, as verified by ultrasound post-weld. The ideal downward force of 7.3 kN was then applied to the plates studied. To ensure industry-quality welds, each plate was scanned via ultrasound, as shown in Figure 88 for Plate A. Figure 88a displays the A-scan with amplitude readings and depth. The initial 0.4 mm is an artifact of the ultrasound, so the top surface is considered to start at 0.4 mm, though this

section is unreliable. This unreliability is due to the surface irregularities produced by FSW, which create ripples that distort the A-Scan signal quality. These distortions cause unreliable depth readings in the first 1.0 from the surface, leading to a "loss of inspection" in this initial depth range. When inspecting for defects, the presence of a high-noise signal can create a "dead zone." This dead zone refers to areas where the ultrasound signal is too noisy to interpret, typically affecting the initial depths accurately. However, major defects in FSW welds usually occur deeper, in the final 1/3 of the pin length, which is outside this dead zone. Therefore, while the initial depths are compromised by noise, the overall defect detection remains effective.

Beyond the 1 mm mark, the data becomes reliable, indicating low amplitude readings throughout the weld, as shown in the orange box in Figure 88b. The initial high-amplitude signal on the A-Scan indicates the ultrasound wave touching the top surface of the material. Misinterpretations of this signal can lead to incorrect assumptions about where the weld starts or the depth of various features. The B-scan provides a longitudinal view of the depth, with colors corresponding to the A-scan amplitudes (see color bar in Figure 88a). The initial 1 mm shows unreliable readings due to the dead zone. Below this region, the consistent green-yellow color indicates low amplitude or near-zero readings, suggesting the absence of significant defects. Any notable defects would be indicated by intense red or blue colors, which signify voids. The C-scan, included for completeness, outlines the top surface. Although the C-scans show high amplitude readings, they are unreliable due to the previously mentioned "dead zone." For example, referring to the crosshairs in Figure 88d, a negative high amplitude signal is read on the surface; however, following the crosshairs to Figure 88c reveals no defects at the actual weld joint. Therefore, the irregular colors across the C-scan do not accurately represent the internal sound structure of the weld.

In summary, the unreliability of depth readings in ultrasound inspections of friction stir welds is primarily due to surface irregularities and high-noise signals, which create a dead zone. This dead zone affects the initial 1.0 mm from the surface, making it challenging to accurately determine the starting point of the weld. However, the method remains effective for detecting deeper defects, thereby ensuring the overall integrity of the weld and confirming its quality.

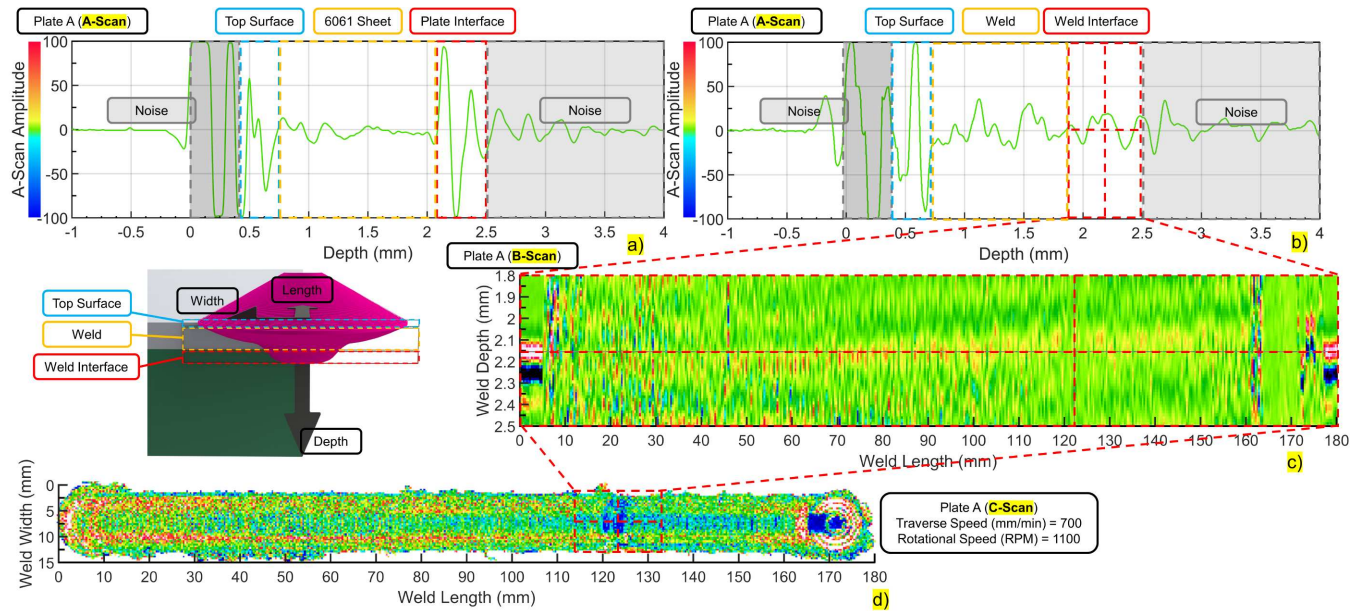


Figure 88: Ultrasonic non-destructive evaluation of friction stir welds; (a) reference plate scan (b) one-dimensional display of amplitude versus depth; (c) cross-sectional view revealing defects across the length and depth; (d) external details of the weld projected onto a plane

The last parameter requiring consideration is consistent fixturing, which is also considered for comparability between plates. It has been noted that the manner in which material is constrained during welding and how it is released post-welding can influence distortion and residual stress. For instance, a study by Costa et al. [193] demonstrated that altering the releasing strategy from counterclockwise (CCW) to clockwise (CW) does impact residual stress and plate distortion, although the effects are relatively minor. Therefore, the plates in the current study maintained uniformity in clamping configurations and release schedules for each set, as shown in Figure 89. The clamping loads for each set also remained consistent at 680 lbs downforce across all clamps. Lastly, temperature measurements and simulations are completed for this study, as temperature and temperature gradients are essential to texture generation, plastic deformation, and residual stress. For each plate, two distinct temperature measurements were acquired as positions A and B (see Figure 89). The thermocouples were installed to ensure accurate readings during the welding process, and measurements were acquired at a 100Hz rate utilizing the temperature measurements Intertechnology system. These temperature measurements were required to verify an FSW simulation utilized in the present study.

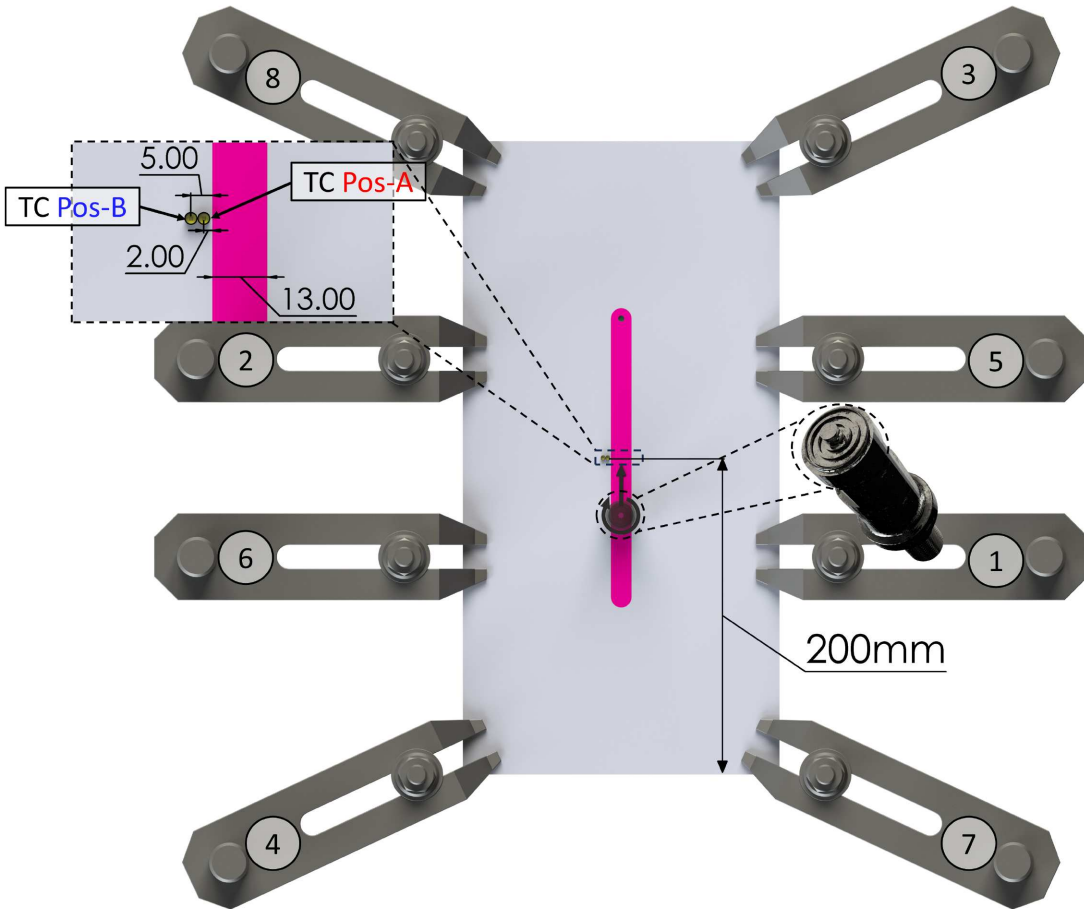


Figure 89: Plate clamping setup, release order, thermocouple (TC) positions, and tooling

4.1.1.2 Smoothed Particle Hydrodynamics

FSW is a complex solid-state joining process characterized by intricate material flow patterns, large plastic deformation, and substantial temperature gradients (Fraser et al. [35]). Capturing these dynamics requires a refined computational framework. Traditional mesh-based methods, though prevalent, often face challenges when simulating FSW. Their dependency on a predefined mesh means that when materials undergo large deformations, the mesh distorts, leading to errors or even simulation failures (Fraser et al. [35]). The complexity of FSW, with its dynamic material flow and large deformations, makes these methods less suitable. In contrast, the mesh-free approach offers flexibility. Since it isn't anchored to a fixed mesh, it navigates the challenges of large plastic deformations. Within this approach, the Lagrangian framework plays a crucial role. Unlike Eulerian methods, in which calculations are performed on a fixed spatial grid, the Lagrangian perspective follows individual material points as they move and deform (Fraser et al. [35]). This

fluidity appropriately captures the material's movement during FSW without being hindered by mesh-related issues.

Smoothed Particle Hydrodynamics (SPH) is one of many mesh-free approaches. Each material point possesses attributes such as mass, velocity, and temperature. The SPH method is especially effective in capturing the dynamic material flow in FSW. Therefore, the conservation of mass, momentum, and energy is ensured by the solution of the conservation equations in the mesh-free framework. The material points move and interact without the constraints of a fixed mesh, making it adept at simulating the large plastic deformations present in the FSW process (Fraser et al. [35]).

The detailed relationship between temperature and material deformation in FSW cannot be understated. Aluminum, for instance, exhibits a temperature-dependent behavior; its mechanical properties, like strength and ductility, can change with varying temperatures (Fraser et al. [35]). This relationship necessitates a fully coupled thermo-mechanical approach where temperature and mechanical deformation are intertwined. The model employed here has been optimized for aluminum. It capitalizes on specific behaviors of aluminum, such as its temperature-dependent flow stress, ensuring accurate simulation results (Fraser et al. [35]).

Validation is paramount to ensure the model's predictions align with real-world observations. This study monitored temperatures during FSW using thermocouples positioned away from the weld, drilled approximately two millimeters apart (Fraser et al. [35]). This setup ensured that the thermocouples captured the temperature gradients without being affected by the direct action of the welding tool. The simulation's temperature predictions were then accurately compared with these measurements, affirming the model's accuracy.

4.1.1.3 Residual Stress Measurements with Neutron Diffraction

For the plates utilized in Chapter 4, neutron diffraction experiments were conducted on the Kowari-Strain Scanner instrument at the Australian Centre for Neutron Scattering. Again, the specific methods utilized in the present study to acquire strain were applied by Sediako et al. [15], employing the equations established by Hutchings et al. [115] to determine the stress in all three orientations (longitudinal, transverse, and normal) on the friction stir welded plates. Furthermore, the breakthrough work by Holden and Schajer [124] formulated in detail the method of experimental elastic strains and stresses in terms of lattice interplanar spacing measurements.

Please refer to the section “2.4.3.2 The Peak-Shift Method in the Calculation of Residual Stresses” for the specific methods utilized to calculate residual stress.

4.1.1.4 Texture Measurements with EBSD

Texture data was obtained with the Tescan Mira 3 XMU Scanning Electron Microscope (SEM) at the University of British Columbia, where the key technique used in this study was Electron Backscatter Diffraction (EBSD), facilitated by an Oxford Instruments Nordlys EBSD detector. EBSD is a SEM-based technique that provides crystallographic data about the microstructure of a sample. It involves the interaction between an electron beam and a slanted crystalline sample, where the diffracted electrons form a pattern that can be identified. This pattern provides information about the phase and the crystallographic orientation, effectively reconstructing the microstructure. Please refer to section “2.5 Measuring Texture in Friction-Stir-Welding”, for a brief overview of the EBSD process.

4.1.2 Torque Analysis

Several process parameters are crucial in FSW, including rotational speed, traverse speed, plunge force/axial force, and the tool geometry utilized. Among these, torque is particularly significant and is closely dependent on the tool geometry (see Figure 89 for the tool). For instance, a study by Aldanondo et al. [48] demonstrated that with constant welding speeds, a change in tool geometry can significantly alter the spindle torque. When the tool geometry is held constant, the torque provides valuable insights into the welding process, reflecting the interaction between the welding tool and the workpiece. FSW generates heat through friction, which softens the material and allows for stirring without melting. Torque is a direct measure of the resistance the tool encounters as it moves through the material. Figure 90 illustrates that the torque required diminishes as the tool's rotational speed escalates. This reduction occurs as the increased rotational speed induces a temperature rise, consequently softening the material and facilitating easier rotation. This observation aligns with the findings reported by Arora et al. [194].

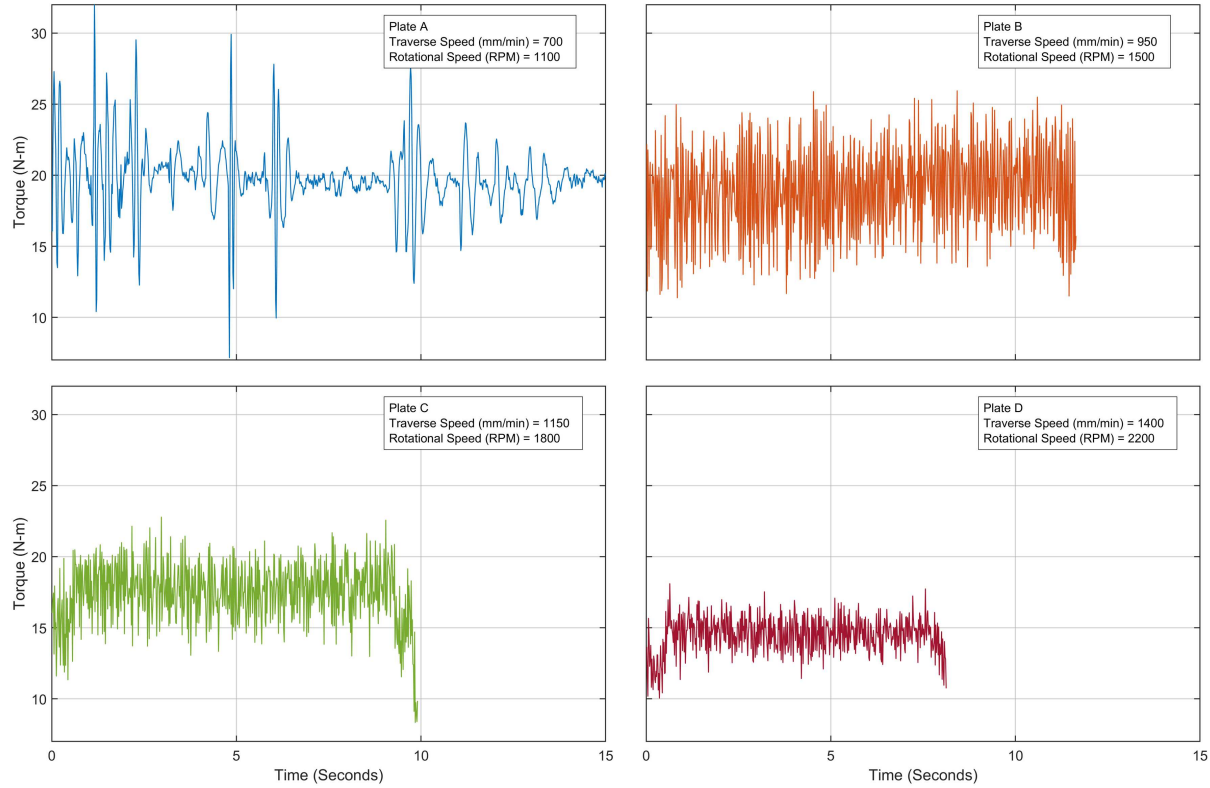


Figure 90: Plate's A, B, C, and D spindle torque measurements

The material's malleability and flow during the welding process are closely linked to the tool's stirring effectiveness. Changing torque by differing welding speeds causes the tool's material flow, material adhesion, and element segregation to change, impacting the weld's microstructure, mechanical properties, texture, and even lattice parameters. Medina et al. [195] discovered that the distinct microstructural zones of FSW materials, characterized by grain size, texture, and lattice parameter (reflecting residual stress), closely align with temperatures near the pin, as determined through molecular dynamics simulations. These near-pin temperatures closely align with torque and depend on welding speeds (Long et al. [196]). Examining the mean spindle torques in Figure 90, Plate A exhibits the highest value at just under 20 Nm, Plate B at almost 19 Nm, Plate C near 17 Nm, and Plate D at 14 Nm, illustrating that as the welding speeds increase, the average torque required decreases. Additionally, Figure 90 illustrates that as the tool welds the material, it moves from areas of high maximum temperature (requiring less torque) to zones of lower temperature (requiring more torque), thereby forming distinct regions across the weld. These regions manifest as variations in texture, which arise due to the asymmetry inherent in the FSW process. The process is characterized by an advancing side, where the tool's forward motion aligns with its rotational

direction, and a retreating side, which moves in the opposite direction to the tool's rotation. In this study, the advancing side is always depicted to the left of the weld's center. These texture variations play a crucial role in determining the mechanical properties, such as yield strength, which in turn influences the residual stress response observed in the weld.

4.1.3 Smoothed Particle Hydrodynamics Simulations

Smoothed Particle Hydrodynamics (SPH) constitutes a mesh-free numerical approach initially developed by Gingold et al. [197], as well as Lucy et al. [198], who worked independently. The technique offers advantages in simulating solid mechanics problems, particularly those involving large plastic deformations, which are especially difficult to handle via traditional methods like the Finite Element Method (FEM). One of the inherent advantages of SPH lies in its mesh-free nature. The spatial derivatives in SPH are calculated via interpolation involving neighboring nodes within a specific radius of influence around the material point in question. The technique relies on solving a set of conservation equations for Computational Solid Mechanics (CSM) simulations in a Lagrangian frame of reference.

In the present study, an SPH model for large plastic deformation serves as the computational framework for simulating FSW processes. The simulation model focuses predominantly on the welding assembly's top plate (6061). This decision was informed by the minor mechanical deformations observed in the bottom plate (A365), which could be neglected without significant loss of accuracy. However, it's crucial to note that the bottom plate has not been entirely disregarded; it remains a vital component in heat transfer calculations. The top plate is discretized using SPH elements, whereas the tool is modeled using rigid finite elements. Lastly, the interaction between the tool and the SPH-discretized plate is governed by a contact algorithm (Fraser et al. [199]).

This study includes three specific cases—labeled Plate B, C, and D—to investigate the welding process. All three scenarios have consistently increased speed parameters while maintaining consistent speed ratios to evaluate the influence of these parameters on the thermal profiles. Thermocouples, designated as TC-A and TC-B, were intentionally placed at locations consistent with the experimental setup. One of the noteworthy aspects of this simulation is its temporal boundary, defined as the "end of the advanced phase." This boundary signifies a time frame

equivalent to three to four seconds of real-world welding. While the computational model could extend this period, constraints on computational resources and time prevented further exploration. However, the existing model provides sufficient data for predictive curve-fitting exercises that could extend the results temporally (see Figure 91).

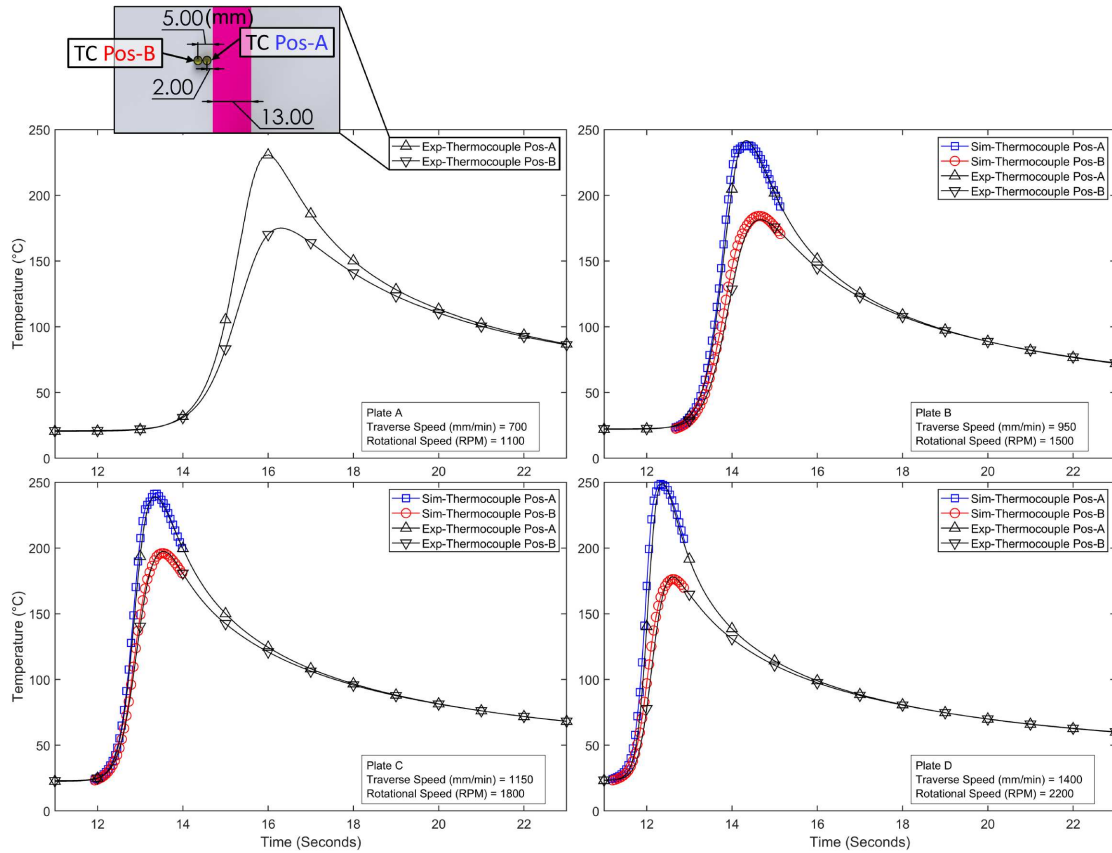


Figure 91: Experimental temperature recording compared to SPH simulated temperature

Due to inherent variations in the drilling process used for thermocouple placement, a perfect alignment between the holes in the four plates is unattainable. Consequently, positions A and B, as depicted in the simulation model, strive to emulate their experimental counterparts in Figure 91. However, the variation in hole location restricts the opportunity for direct comparison between positions A and B across all four plates in Figure 91. Nevertheless, Figure 91 serves a specific and valuable function: it provides strong evidence that the simulated values align remarkably well with the experimental results for plates B, C, and D. This close correspondence underscores the fidelity and reliability of the SPH simulation model employed in this study, particularly in capturing the thermal dynamics of the FSW process.

Having established the validity of the SPH simulation model through experimental measurements, the stage is set for comparisons within the simulated framework. Figure 92 illustrates the material points in the SPH model, the deformation caused by the initial input of the Friction Welding tool, and the tool's velocity magnitudes. The highest tool velocity occurs at its maximum radius, gradually decreasing towards the center. The advancing side, identified on the left in Figure 92, aligns with both the traversing and rotational vectors. Conversely, the retreating side, shown on the right, opposes the advancing side.

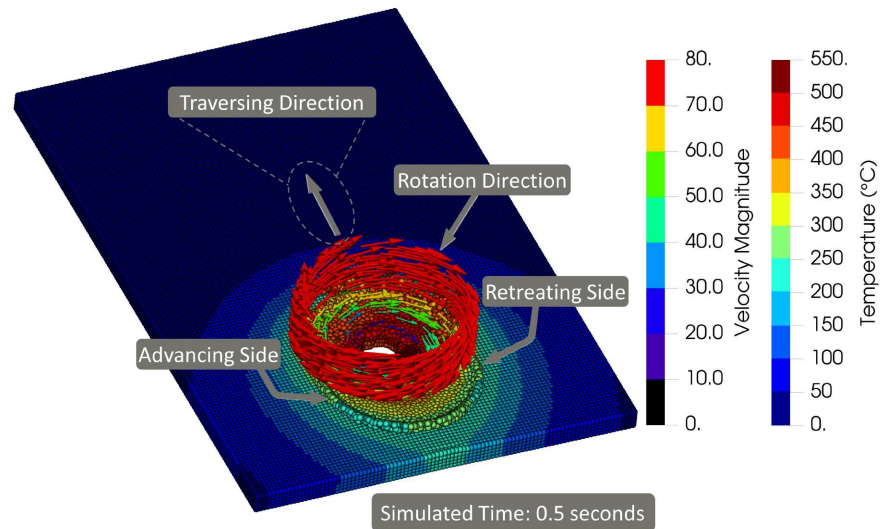


Figure 92: SPH simulation environment for friction stir welding

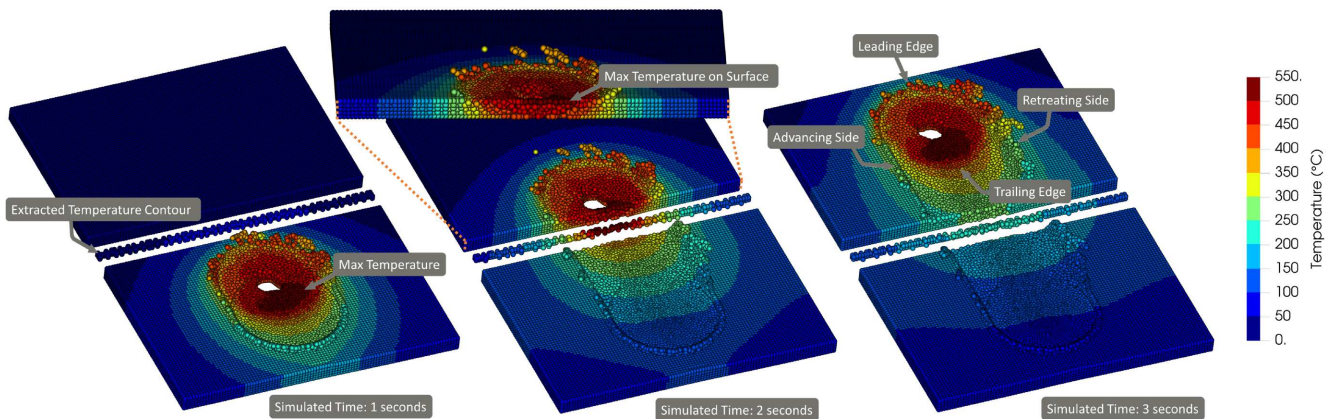


Figure 93: Progression of the friction stir welding simulation with the isolated strip for temperature contour

To facilitate a direct temperature comparison between the plates simulated, a strip of material across the weld (see Figure 93) is isolated; at 39 locations throughout the strip, the temperature is

individually recorded over the entire simulation. Data are compiled to create a temperature contour over time, depicting the temperature changes a strip of material undergoes as the FSW tool approaches and moves through it (refer to Figure 94).

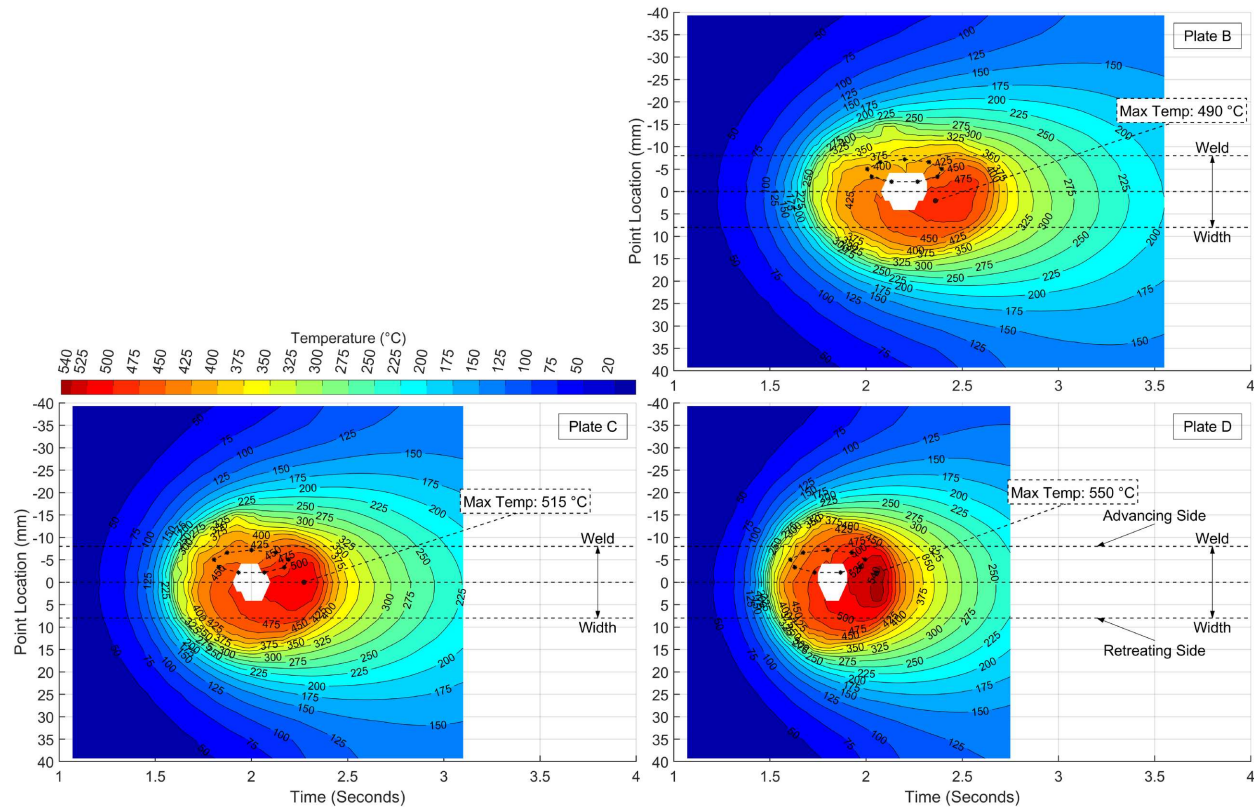


Figure 94: Temperature contour illustrating the thermal gradients of the stationary material over the simulation period for friction stir welded Plates B, C, and D

In SPH simulations, material points move and deform, preventing any overlap between the rigid body of the FSW tool and the SPH simulation material points. This results in a section of displaced material, visible in Figure 93, and an area where no temperature is recorded, as shown in Figure 94. Initial observations reveal marked differences in the widths and breadths of the temperature profiles. Plate B showcases the widest temperature breadth in the temporal extension, a characteristic that gradually contracts with increasing speeds in Plates C and D.

With an increase in operating speed, the thermal exposure of the material is compressed temporally (see Figure 94). The increasing speed of the process from Plates B to D restricts the duration of thermal diffusion, leading to a concentrated heat zone. This effect is significant for torque behavior during FSW. Elevated rotational speeds localize the heat, shortening the interval for material cooling. This thermal confinement, coupled with the rise in peak temperatures—plate B reaching

490 °C, plate C at 515 °C, and plate D peaking at 550 °C (see Figure 94)—stabilizes torque responses throughout the welding process (see Figure 90). Such stabilization directly influences the flow stress of the material. Lower temperatures correspond to reduced ductility of the welding material, resulting in increased flow stress and torque, a factor that substantially affects the texture, as elaborated in section “4.1.4 Residual Stress on Single Welded Plates”. This link between thermal conditions and mechanical resistance aligns with the findings of previous researchers, Yan et al. [200].

Controversy exists in the literature regarding the precise location of peak temperatures during FSW. For instance, simulations by Hamilton et al. [201] suggest that the advancing side of the weld is 5 to 10 K warmer than the retreating side. Conversely, thermocouple experiments conducted by Silva-Magalhães et al. [34] detect the maximum temperature on the retreating-trailing edge of the FSW tool. However, temperature differences between the advancing and retreating sides are significantly influenced by process parameters and are specific to the joint configuration and tool geometry. Even when all other conditions are constant, a change in welding parameters can modify these temperature differences, making comparisons between studies challenging. In the current study, the contour map in Figure 94 illustrates this temperature peak occurring as the FSW tool traverses over the material, producing the maximum temperature zone trailing the tool. This result is in line with the findings of Silva-Magalhães et al. [34].

Despite the advancing side not reaching maximum temperature, it presents an intriguing thermal profile. Figure 94 illustrates pronounced temperature gradients near the tool pin, particularly in plates B and D. The advancing side initially encounters cooler material, meets the heated zone at the tool's trailing edge, and then quickly cools as the tool continues its path. This characteristic, consistent across all plates but most marked in plates B and D, may be crucial for understanding the distribution of residual stresses discussed in the subsequent sections.

The hypothesis is that within a single set of weld parameters, temperature gradients are more pronounced on the advancing side (see encircled asterisks in Figure 94), potentially leading to increased tensile stresses due to larger misfits, as opposed to the retreating side. According to Hutchings et al. [115], residual stresses arise from shape misfits between different parts, regions, or phases of a component when they do not match in their unstressed states. These misfits can occur over a broad range of scales. For example, large-scale stresses might result from processes

like plastic bending or the sharp thermal gradients encountered during welding or heat treatments. In contrast, smaller-scale stresses may develop between different phases in a composite material due to their distinct properties. The causes of these stresses are varied and can include thermal or elastic misfits. They are influenced by the size of the misfitting region, which dictates the range of stress variation and the extent to which the stress self-equilibrates.

In the context of FSW, these categorizations are especially pertinent. This thermomechanical process, characterized by significant forces, bending, and thermal gradients, along with the involvement of different material phases and regions, comprehensively influences the final residual stress profile in the welded component.

It's these temperature gradients, rather than solely just the peak temperatures, which may influence the misfit and, consequently, the residual stress within a weld. Across different welding parameters, as the welding speed escalates, the maximum temperature achieved plays a crucial role in increasing the thermal gradient and, thus, the peak residual stresses, which is elaborated in section “4.1.4 Residual Stress on Single Welded Plates”. It is important to note that localized heating typically induces only transient residual stresses which dissipate upon cooling to ambient temperatures. However, if the temperature gradient is sufficient to cause plastic deformation, this results in a permanent misfit and a corresponding residual stress (Hutchings et al. [115]). Nevertheless, FSW is inherently a thermomechanical process where the outcome depends on factors such as the base material, plate dimensions, the heating effect of the tool, plate fixation, the strain the tool imparts on the material, and the flow of material within the weld. These factors collectively suggest a significant interrelation among weld texture, thermal profiles, mechanical resistance, and the resultant residual stress.

The SPH simulation's last notable finding underscores that although Plate D reaches the highest temperatures among the tested welding parameters, it registers the lowest cumulative thermal exposure (see Figure 95). In the context of welding, the term "short-term thermal exposure" refers to the immediate or rapid effects of heat on materials within a brief duration—often measured in seconds or minutes rather than hours. This concept can help in understanding how different welding speeds or techniques affect the thermal properties and structural integrity of materials. For instance, Yadav et al. [202] explore how short-term thermal exposures differ between FSW and gas metal arc welding and how these exposures influence stress corrosion cracking through effects

like grain coarsening and softening in the heat-affected zone. Focusing on FSW, altering the welding speed can significantly impact the heat input and, consequently, the thermal exposure of the material. Slower welding speeds typically result in higher heat input as the heat source remains in contact with the material for a longer duration.

Short-term thermal exposure can be quantified by a Time-Temperature Integral (TTI), calculated as the area under the temperature versus time curve for a specific process (Lopatin et al. [203]). This metric expresses the total heat input to a system at varying temperatures over a given period. It is beneficial for assessing the effects of thermal processes where both the magnitude and duration of temperature exposure are critical. In welding, the TTI can help predict changes in material properties such as microstructure transformations, hardness, and residual stresses. For example, if a point has a higher TTI value, it indicates exposure to higher temperatures for longer durations relative to points with lower values. Plate B, due to slower welding speeds, exhibits the largest TTI value.

Moreover, TTI values do not necessarily correlate with thermal gradients, which describe how quickly temperature changes from one point to another within a material. Points exposed to rapid, intense heat, such as quicker welding passes (e.g., Plate D), can experience sharp thermal gradients despite lower TTI values because the temperature rises and falls very quickly. Conversely, a more prolonged and moderate heat application might yield higher TTI values but smoother thermal gradients, allowing more time for heat to conduct away from the heat source and leading to a more even temperature distribution, as observed in Plate B.

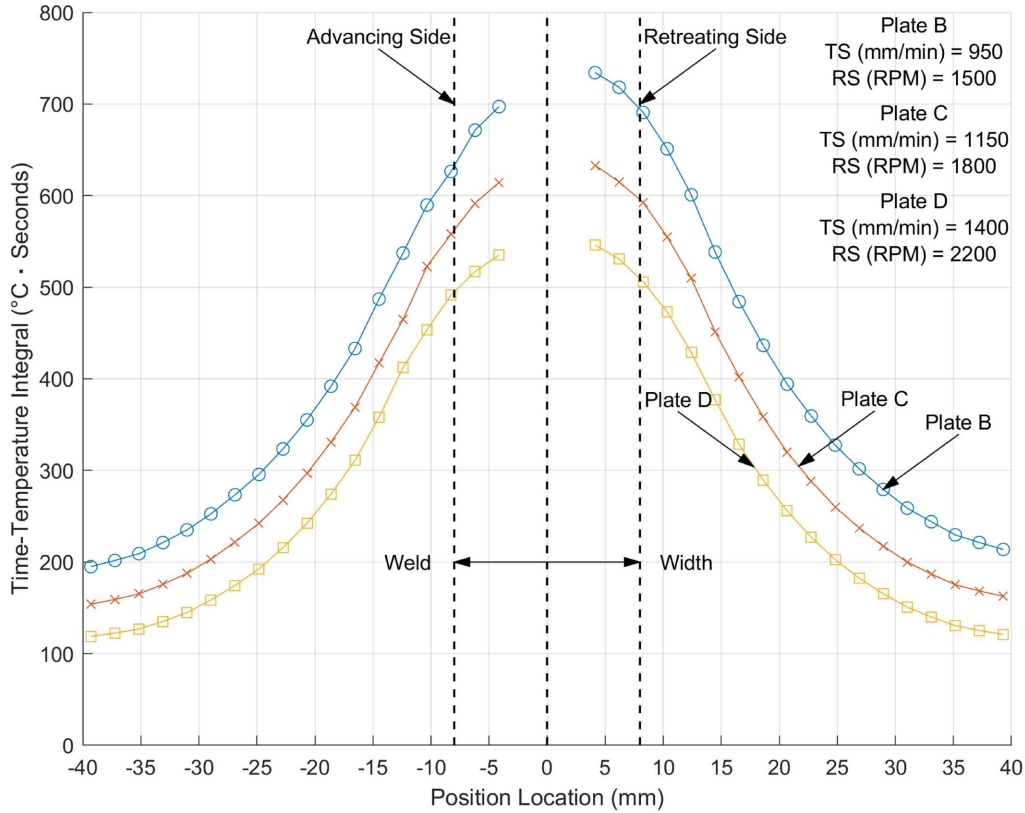


Figure 95: Time-Temperature Integral (TTI) values expressing thermal exposure for each plate simulated

This observation supports the previously discussed idea that higher welding parameters elevate peak temperatures, creating a more pronounced temperature gradient and reducing cumulative thermal exposure. This finding aligns with the research of Lomolino et al. [204], who demonstrated that high welding speeds correlate with low heat inputs, resulting in faster cooling rates of the welded joint. These alterations in thermal profiles may substantially impact the evolution of residual stresses, leading to very different stress profiles, an aspect examined in section “4.1.4 Residual Stress on Single Welded Plates”.

4.1.4 Residual Stress on Single Welded Plates

A critical component of residual stress analysis involves determining stress-free reference values (d_0), which is important in calculating residual stresses. In the present study, it has been noted that welding parameters, in this specific case, the rotational and traversing speeds, affect these d_0 values. Variations in these parameters can change how dissimilar alloys intermix within the weld, resulting in different d_0 values. Consequently, the study included measurements of stress-free lattice spacings at various points across the weld, as illustrated in Figure 96. This figure shows

that despite differing welding conditions, the variation in d_0 values tends to be limited, with some degree of overlap observable when considering standard deviations.

The variations in d_0 values observed across different scans result in an "apparent strain" between the central location and the unaffected metal at the weld's periphery. This strain ranges from 150 to 200 microstrains and, if unaddressed, could impact stress calculations. Therefore, it is essential to determine the d_0 values for each scanned location across the weld to ensure accurate assessments. Notwithstanding this overall trend, discrete exceptions are present, notably at specific locations within the welds. These locations are shown in Figure 96, with the pronounced variability in d_0 values across different weld regions. The largest d_0 values are consistently identified at the central position at the 0 mm location within the weld in the Dynamically Recrystallized Zone (DXZ). This zone represents an area of complete recrystallization of aluminum grains adjacent to the tool pin interface, highlighting a region of intense material transformation induced by the welding process.

In contrast, the recorded d_0 values at the periphery of the weld, specifically at the ± 8 mm positions within the Thermo-Mechanically Affected Zone (TMAZ), are markedly lower. This discrepancy underscores the heterogeneous nature of the welding process, reflecting differences in material behavior and properties between the DXZ and TMAZ. The observed discrepancies in stress-free distribution across each weld underscore the relationship between material composition and welding-induced thermal gradients. As previously highlighted, temperature profiles vary between welds and the extent of a single weld. This variability significantly influences the diffusion rates of critical elements such as Si, Mg, Cu, and Zn within the weld zone. The heterogeneity in temperature distribution is a key driver for the differential diffusion of these elements, leading to localized variations in material composition across the weld, as concluded from the results of section "3.2.4 ICP-AES Analysis". The mechanical mixing of the two alloys (top 6061 and bottom A365 substrate), influenced by variations in welding parameters, may also lead to changes in the chemical composition across the weld.

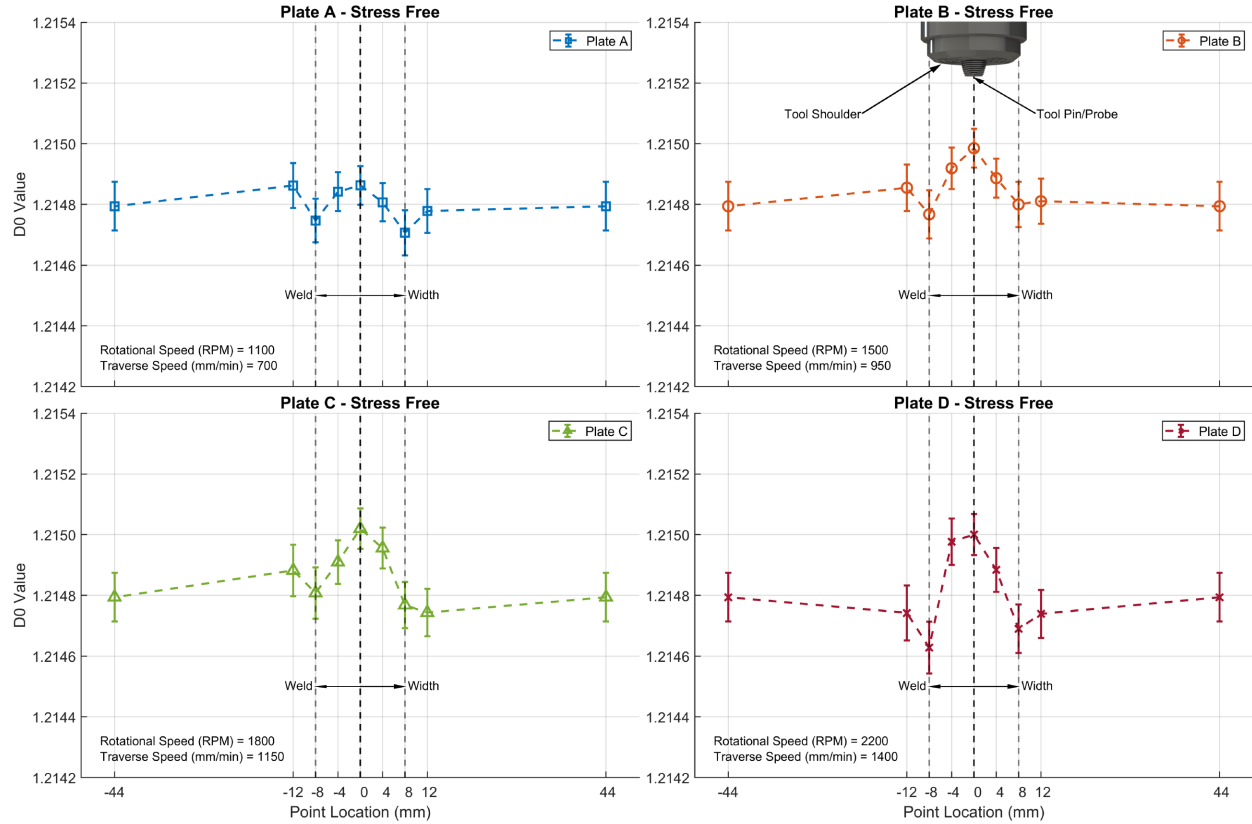


Figure 96: Stress-free reference values (d_0) for welded plates A, B, C, and D

Furthermore, many investigations (Salem et al. [174]; Salem et al. [205]; Moradi et al. [146]) substantiate the phenomenon of particle segregation within the center of the DXZ, often referred to as the flow zone. This segregation occurs as materials are displaced downwards by the action of the rotating pin, which effectively transports particles and fine precipitates toward the core of the DXZ or flow zone (the area directly interacting with the pin). This action is supported by the study by Guerra et al. [206], which found that material was transported downward by the vortex flow of the tool pin. The specific particles being relocated into the center of the stir zone can significantly influence the unstrained lattice parameter. These effects do not necessitate large-scale displacement of materials; even marginal variations in magnesium content can precipitate marked changes in the stress-free lattice spacing, as evidenced in Figure 47 (see section “2.4.3.4 Stress-Free Material”). Similarly, other elements like silicon, prevalent alloying constituents in 6061 aluminum alongside magnesium, play a crucial role in modulating lattice spacing due to differential concentration gradients across the weld. These disparities in elemental distribution contribute to observable deviations in unstrained or stress-free lattice spacing, a phenomenon documented by Barrett et al. [131] (see Figure 47).

The mechanism through which the tool pin facilitates the migration of alloying elements to the weld's center may hinge on the interplay between deformation and diffusion. The lattice structure may incur defects during material deformation, which occurs in welding or mechanical treatment. A notable type of defect is the vacancy, characterized by the absence of an atom in its designated lattice site. The deformation-induced strain prompts atoms to shift, potentially leading to vacancy formation. Deformation-induced vacancies can markedly influence diffusion processes within the material. Consider a scenario in FSW where the operation creates voids or zones enriched with vacancies. These areas serve as conduits for alloying elements, such as magnesium, facilitating their migration through the material matrix. Upon cooling and solidification, these elements may become preferentially concentrated in zones abundant in vacancies. Therefore, the weld's center or its vicinities, subjected to intense deformation, might showcase elevated levels of alloying elements.

A study by Agiwal et al. [207] reveals that the tool pin, in conjunction with the threads on the trailing edge of the friction stir tool, intermittently generates and fills a cavity with each revolution. This phenomenon underscores FSW's intense plastic deformation and localized heating characteristic, whereby the moving tool generates heat and stirs the material, leaving a cavity in its wake. This cavity temporarily harbors excess vacancies—regions bereft of atoms caused by the lattice undergoing strain and reconfiguration. Such vacancies are conducive to the diffusion of alloying elements. As the weld progresses and the cavity is sealed, these transient vacancies evolve into microscopic voids that could become preferential diffusion channels. Consequently, alloying elements like magnesium might congregate within these voids, precipitating localized variations in concentration. These changes, in turn, influence the unstrained or stress-free lattice spacing, particularly at the tool center, compared to the shoulder region, as depicted in Figure 96.

Accounting for stress-free measurements is critical for accurate residual stress evaluation in materials, particularly following processes like welding. Variations in stress-free lattice spacing directly influence the measurement of residual stress. This adjustment is essential for achieving precise stress measurements. Moreover, whether tensile or compressive, residual stresses significantly determine a material's mechanical properties and structural integrity. Properly considering stress-free variations at the outset allows for a more accurate interpretation of these residual stresses, ensuring their effects on material performance are correctly understood. Lastly,

failing to recognize the variations in stress-free lattice parameters can lead to misleading stress measurements. In friction stir welds, neglecting these variations might cause an underestimation of tensile strains, particularly near the weld centerline. Therefore, recognizing and accounting for these variations is essential to avoid measurement inaccuracies and ensure the correct evaluation of residual stresses. The findings align closely with those of Peel et al. [11], who demonstrated that employing a single, unstrained reference piece (d_0) for all positions across the weld could lead to discrepancies as significant as ± 150 microstrain. In this study, a discrepancy of ± 200 microstrain is observed between different regions within the weld. The impact of this discrepancy on stress calculations varies significantly depending on the actual stress levels present. Consequently, the percentage impact of the same d_0 discrepancy can vary greatly, making it difficult to compare these results with those from other studies directly. Upon emphasizing the importance of obtaining accurate stress-free reference values, the focus shifts to examining the stresses calculated from measurements along the weld's longitudinal direction, as depicted in Figure 97.

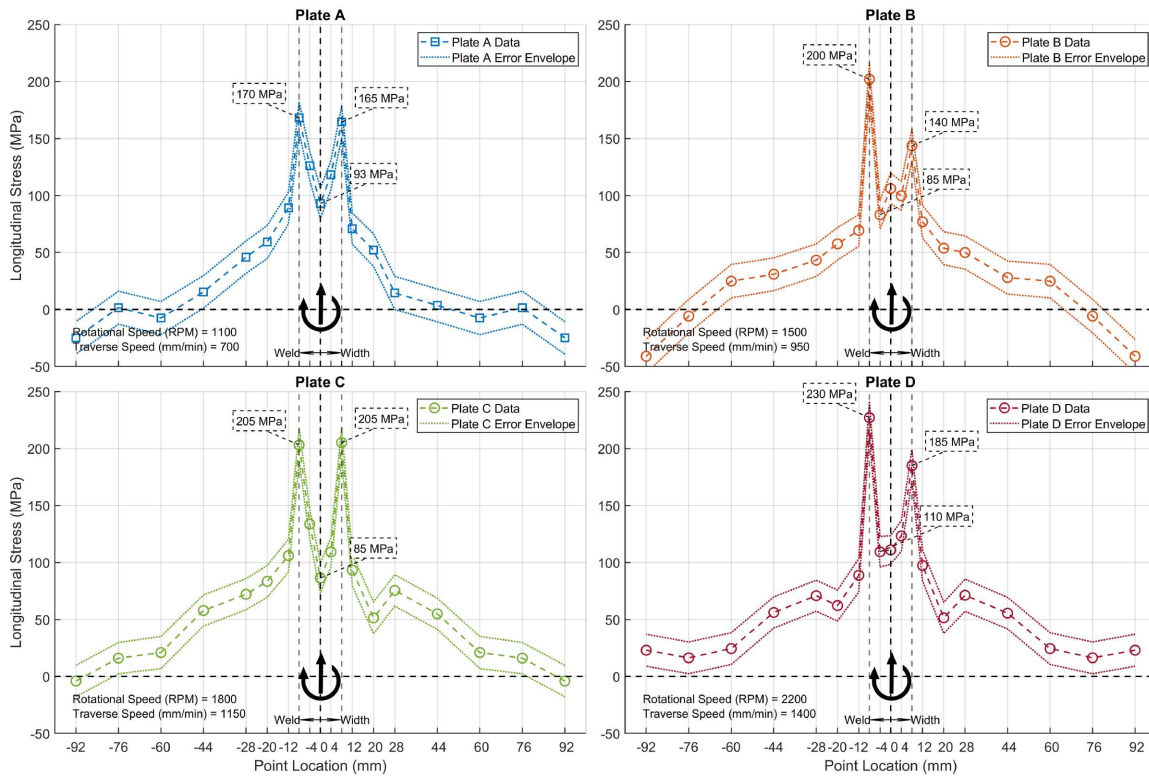


Figure 97: Longitudinal stress from the center of each friction stir weld on Plates A, B, C, and D

This analysis reveals a characteristic M-shaped stress profile across all examined plates (A, B, C, and D) observed at the weld's center. This M-shaped profile is marked by a low-stress point in the

dynamic recrystallization zone within the center of the weld—93 MPa for Plate A. Stress levels increase towards the retreating and advancing sides, with measurements of 165 and 170 MPa, respectively, for Plate A. The M-shaped profile is attributed to varying welding temperatures and their cooling mechanisms, which induce different thermal contractions across the weld. The formation of residual stress in FSW is primarily due to the thermal stress cycle the material undergoes during the welding process. Initially, in front of the weld zone, a large area of compressive stress develops due to the thermal expansion of the warming material being constrained by the surrounding cold material. Where stress exceeds the compressive yield stress, plastic flow occurs. As the material cools and contracts, a region of tensile stress forms behind the weld zone, again being restrained by the surrounding cold material. If the stress level exceeds the yield stress, plastic flow will occur in this region as well. The magnitude of the maximum tensile stress developed during this process is intrinsically tied to the yield strength of the material. Specifically, as the material cools and contracts, the tensile stress increases. If this tensile stress reaches a point where it exceeds the material's yield strength, it induces plastic deformation, which is typically presented as component distortion. This means that the yield strength of the material not only serves as a threshold for the onset of plastic deformation but also directly influences the peak tensile residual stress that can be developed. This relationship underscores the importance of the material properties, particularly the yield strength, in determining the final residual stress profile after welding. Analyzing the trends of stress in both the transverse and normal directions, as depicted in Figure 98 and Figure 99, reveals a notable pattern: an increase in weld traverse speed correlates with a rise in average stresses across the entire plate, however much lower compared to the longitudinal residual stress direction.

The maximum stresses occur in the longitudinal direction presented in Figure 97 (parallel to the welding direction) for multiple reasons, the first of which is the rigid clamping used in FSW to hold the pieces in place during welding prevents contraction of the materials during cooling, leading to the generation of longitudinal and transverse stresses. However, the constraints are more severe in the direction parallel to the welding line, resulting in higher longitudinal stresses.

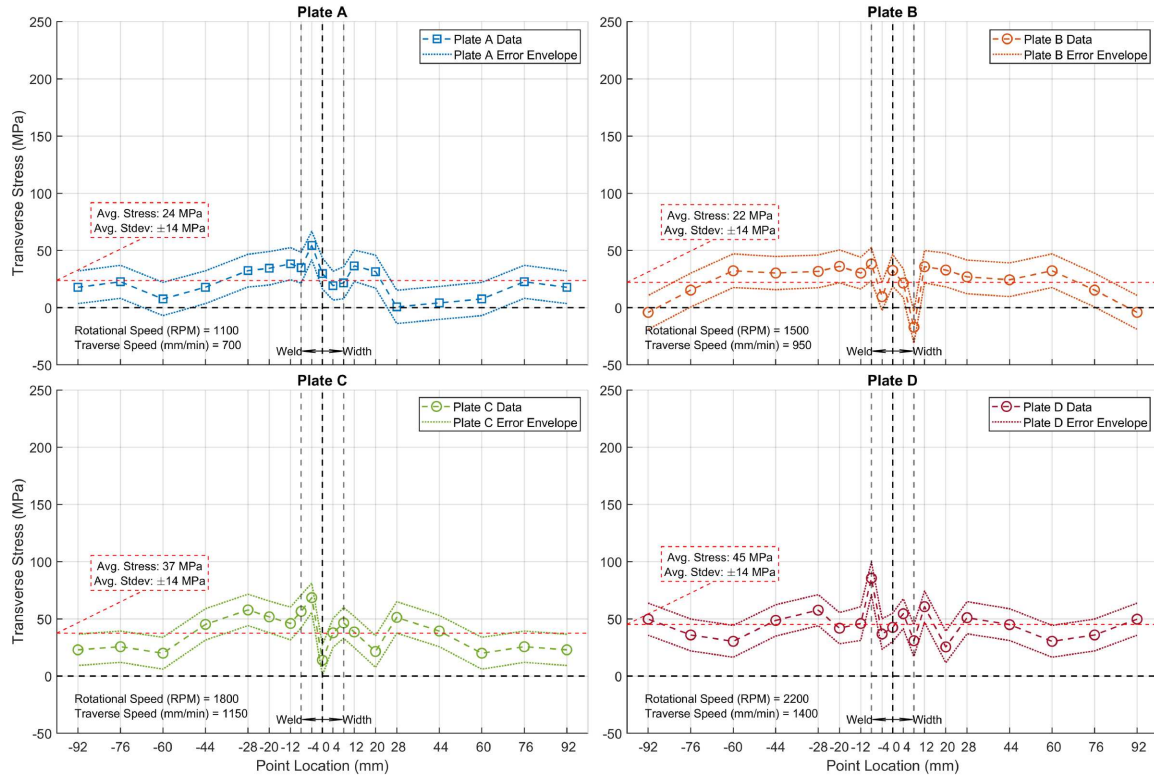


Figure 98: Transverse stresses from the center of each friction stir weld on Plates A, B, C, and D

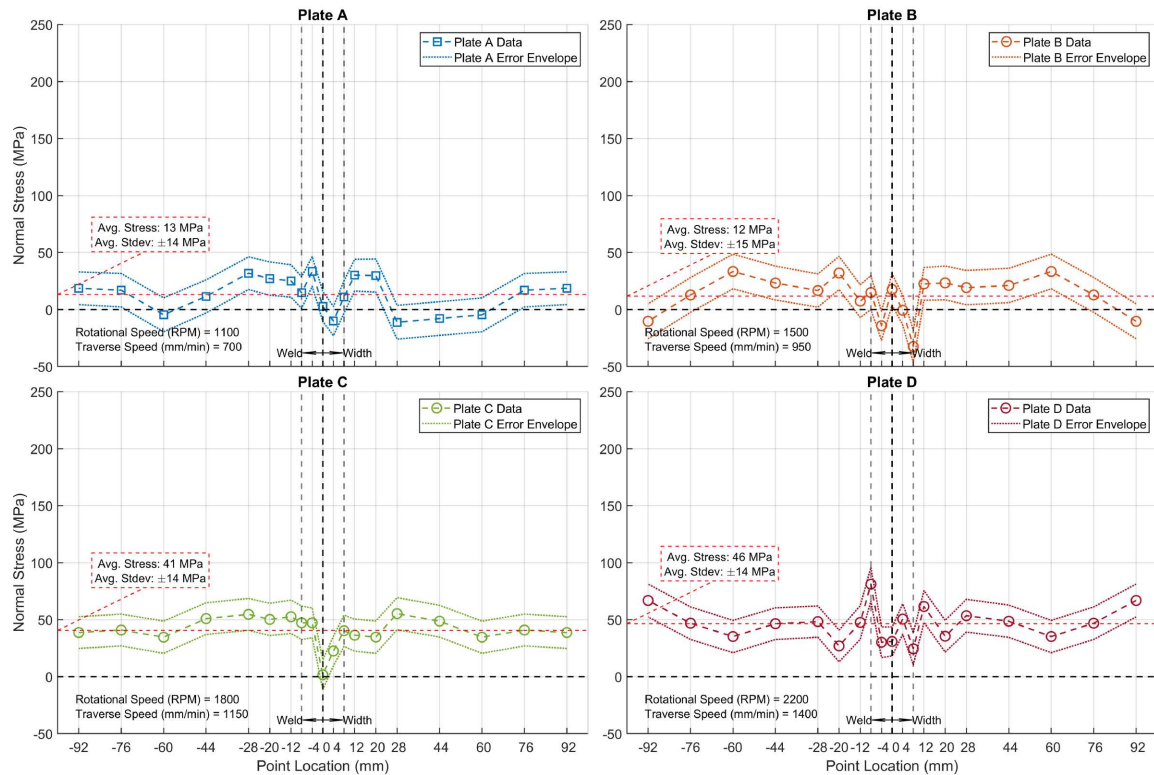


Figure 99: Normal stresses from the center of each friction stir weld on Plates A, B, C, and D

The constraints are more severe in the direction parallel to the welding line because the distribution of heat during the FSW process is not uniform, which affects the residual stress patterns. Heat is localized in the longitudinal direction (see Figure 94); this localized heating forms larger temperature gradients, leading to higher residual stresses in the welding direction. The longitudinal direction, which is the only direction subjected to the traverse motion of the tool, causes intense shear deformation in the material, leading to high levels of strain that contribute to residual stress (Bajaj et al. [208]; Casavola et al. [209]). The lower stress is attributed to the asymmetric shape of the thermal field characteristic of FSW.

The relatively small magnitude of normal stress can be explained by the more uniform temperature gradient in this direction and the absence of restraint from fixturing along this axis (Williams et al. [82]). However, significant variations in transverse stresses are observed at different stages of the weld (see Figure 100)—beginning, middle, and end—predominantly attributed to unique thermal gradients for those regions along the weld. In previous discussions regarding plates A, B, C, and D, residual stresses were measured at the center, revealing a consistent pattern of low, normal, and transverse stress levels. However, in the transverse direction, the stresses are specifically affected by a highly irregular thermal gradient at the start and end of the weld. For instance, at the weld's start for Plate A, where the temperature is at or near ambient, the plunging process introduces significant mechanical deformation and stress due to the plate's lower temperature and the pronounced thermal gradient caused by the tool's rotation during plunging until it reaches its operational depth and temperature. This starting sequence creates a markedly different thermal gradient at the weld's beginning, especially affecting the transverse direction compared to the center. Longitudinal stresses tend to remain relatively stable since, after the welding begins and the tool moves, a similar thermal gradient establishes itself parallel to the weld, producing high tensile stresses in this direction. However, these stresses are slightly lower at the weld's start, likely due to more time for heat dispersion from the starting sequence, making the temperature gradients in the longitudinal direction slightly less intense than in the middle of the welded bead. At the weld's end, transverse stresses show a distinct pattern again, likely due to a completely different thermal gradient experienced as the tool is withdrawn, abruptly ending the heat source and causing higher cooling rates. This rapid change introduces a unique thermal gradient at the weld's end, significantly affecting both longitudinal and transverse stresses. Figure 100 shows that longitudinal maximum tensile stresses notably increase towards the weld's end; however, the profile does not

exhibit any compressive residual stress at the weld's end in any direction, with slightly negative values still falling within the specified level of standard deviation uncertainty.

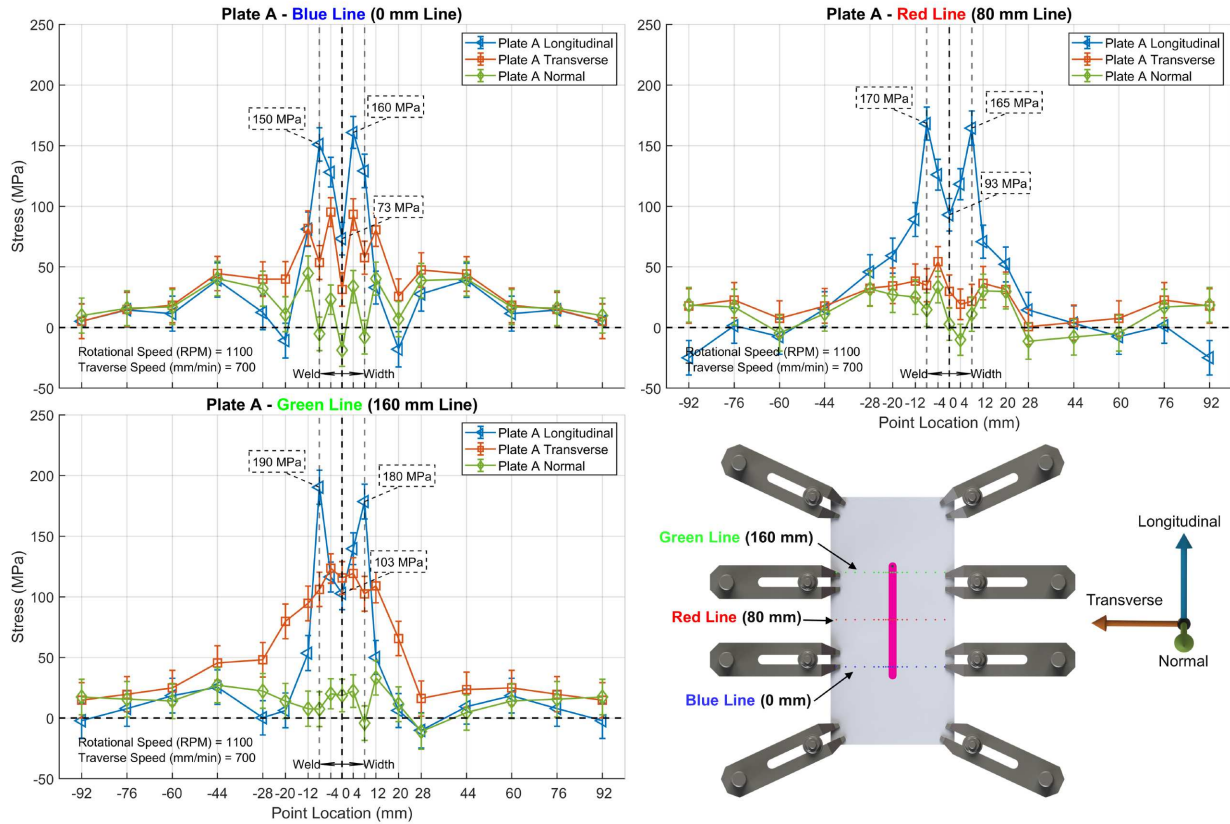


Figure 100: Longitudinal, transverse, and normal stresses from the start to the end of the weld on Plate A.

This observation is significant compared to the transverse stresses; at no point in this direction of stress does the profile become compressive. Purely tensile stress in the transverse direction near the exit hole may have some significant effects; for example, researchers Wang et al. [210] found that in loading, the workpiece would consistently fracture along the exit hole; this was attributed to strictly large tensile residual stresses around the exit hole. These increased/strict tensile stresses may partly be due to the increased transverse stresses measured in the present study, resulting in a combined effect with some of the large longitudinal tensile stresses in the same location. This observation reveals that both longitudinal and transverse stresses play crucial roles in FSW, albeit at different points in the weld, especially since transverse stresses at the weld's end are not only considerable in magnitude but also extend further from the weld compared to longitudinal stresses.

To synthesize the findings presented above, it becomes evident that the welding process, characterized by the intricate interplay of rotational and traverse speeds, significantly influences the residual stresses and plastic strains encountered in the welded plates. An overarching observation is the trend of increasing residual stresses with the escalation of both rotational and traverse speeds, signifying a proportional relationship between welding parameters and the residual stress response. A pivotal aspect of this response is the pronounced plastic strain on the advancing side, attributed to the combined effects of shear strain from the rotational vector and additional strain introduced by the traverse vector. This phenomenon explains why certain plates manifest disproportionately higher strain on the advancing side, thereby underscoring the critical role of the direction of traverse action in dictating plastic strain distribution and the consequent residual stress outcomes.

The analysis further reveals that the residual stress response is intricately linked to the thermal gradients encountered during welding. Specifically, the advancing side exhibits unique and sometimes more intense thermal gradients, as evidenced in the comparative analysis of plates B and D against plate C. Moreover, the examination of transitional regions, such as the entry and exit points of the weld, brings to light the dynamic nature of residual stress profiles across the FSW plates, highlighting the variable stress landscapes from the start to the end of the welding process.

Examining the variance in residual stresses between the advancing and retreating sides of selected plates, as depicted in Figure 97, reveals that plates B and D demonstrate markedly higher stresses on the advancing side. This difference stems from variations in maximum temperature, temperature gradients, cooling rates, and the resulting misfits. Such misalignment is detectable through the results of the SPH simulation because it traces the changes between individual material point positions. The material points in SPH simulations move freely, adjusting to the material's behavior and engaging directly with adjacent material points. This characteristic gives SPH the capability to manage complex shapes and deformations more naturally. Moreover, SPH estimates attributes like density, velocity, and stress by leveraging local data from neighboring material points, making it particularly effective in detecting localized phenomena such as plastic strain (see Figure 101). The strain presented in Figure 101 is the effective plastic strain, which is calculated by subtracting the elastic strain from the total strain. The total strain is determined by measuring how much the material deforms when a load is applied to it. This includes both elastic deformation,

which is temporary, and plastic deformation, which is permanent. Effective plastic strain specifically measures the permanent deformation and is a ratio of lengths (change in length over original length), making it a unitless measure (Fraser et al. [190]). Figure 101 excludes data from the early stages of the simulation, shown by the uncolored material points where the strain is significantly higher, to avoid skewing of the more relevant strain comparisons between the advancing and retreating sides. This adjustment reveals that despite the maximum temperature occurring at the trailing edge of the tool path during welding, the plastic strain observed is substantially greater on the advancing side than on the retreating side. Across all examined welded plates, the peak strain levels remained consistent, yet the extent of the area experiencing high strain expanded progressively with increased welding speed.

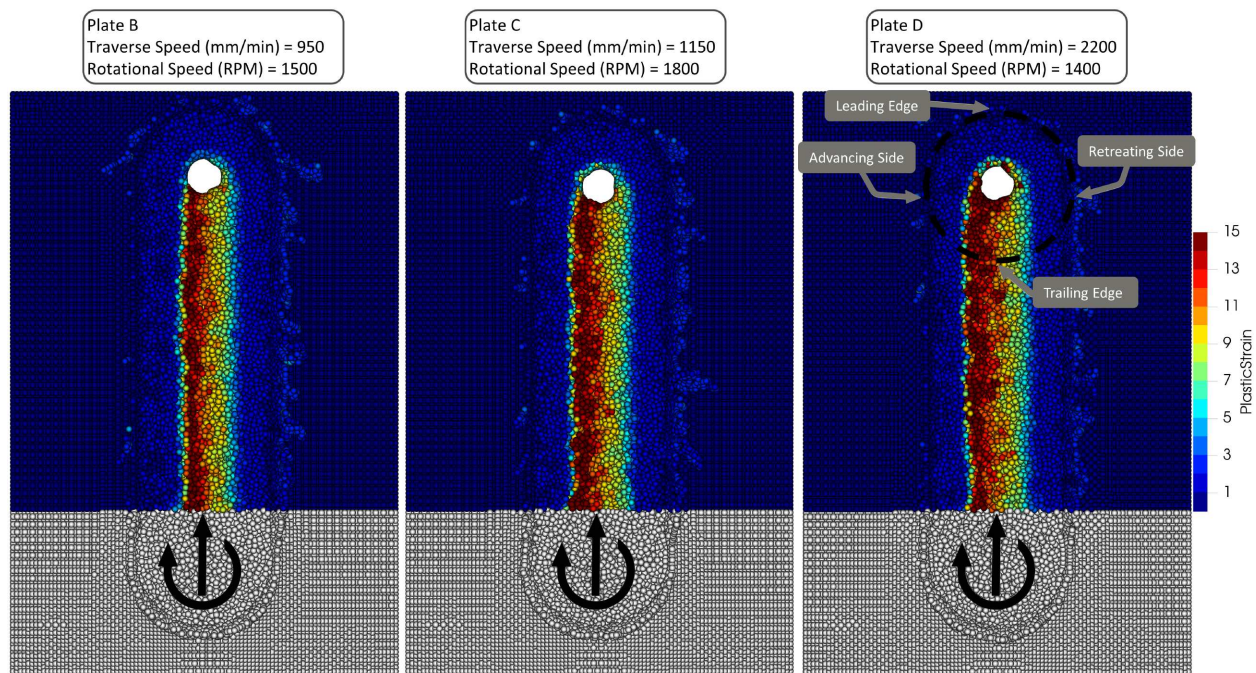


Figure 101: Plastic strain results from SPH simulation of friction stir welds

As mentioned in this discussion of Figure 94, the maximum temperature during FSW is typically found at the trailing edge of the tool. This location is due to the tool's motion, which involves heating the material in front (advancing side) and then passing over it, leaving the maximum temperature area on the trailing edge. This specific maximum temperature results in a temperature gradient, causing a differential expansion and contraction across the weld area. Despite the maximum temperature occurring at the trailing edge, the maximum plastic strain occurs on the advancing side. The maximum plastic strain is primarily caused by the material on the advancing

side being under direct and continuous contact with the rotating tool, experiencing intense friction and deformation before it can cool down. The tool pushes the material to the side and forward, causing extensive plastic deformation as the material is forced under and around the pin. The development of residual stress is closely linked to the material's plastic deformation, which causes heating, followed by its subsequent cooling under constraint. On the advancing side, where plastic deformation is maximal, maximum residual tensile stresses often develop upon cooling (see Figure 97). The material here is stretched and compressed under high heat and then 'locked' into place as it cools, creating tension within the structure. The misfit of plastic strain, particularly between the regions near the pin on the advancing side and the heat-affected zone (HAZ), contributes significantly to the residual stress profile. This misfit arises because different areas of the weld zone cool and contract at different rates due to variations in thermal exposure and mechanical deformation. This differential cooling can lead to areas of tensile and compressive stress, influencing the overall residual stress state. Furthermore, it's important to note that residual stress is a function of the elastic strains that remain after the material has yielded plastically under the tool's influence, which is seen in Figure 97. Whereas the plastic deformation shown in Figure 101 permanently alters the material's shape and volume, while the elastic strain reflects the 'spring-back' or recovery of the material after the tool passes and the temperature decreases. This elastic recovery does not completely return the material to its original state, leaving residual stresses. Qualitatively, this tells us that the material's response to FSW is highly dependent on thermal dynamics, tool path, and mechanical forces. Quantitatively, residual stress profiles can be predicted based on the extent of plastic deformation and thermal gradients, informing decisions on tool design, welding speed, and path to optimize weld strength and minimize distortions or weaknesses.

Bajaj et al. [208] find similar results, stating that the maximum strain is much higher on the advancing side than on the retreating side. The difference in strain experienced between the advancing and the retreating sides can be attributed to the interaction of the traverse vector (direction in which the tool moves along the weld) and the rotational vector (direction in which the tool rotates), which alters the follow flow behavior and the thermal conditions on each side. Serious consideration should also be given to the center of the tool, where most of the material is deposited behind its original position relative to the welding direction; Seidel and Reynolds et al. [211] observed that no material was displaced further than the diameter of the pin. The geometry

of the tool and how the material interacts with its several facets, shoulder, pin, and threads lead to the formation of unique zones characterized by specific flow behaviors. Schmidt et al. [212] identified a zone close to the pin, known as the rotational or flow zone. On the tool's advancing side, the material flow is termed advancing flow, which, according to Bajaj et al. [208], circles the pin several times prior to deposition, thereby experiencing increased shear strain. Other zones have been identified by Guerra et al. [206], emphasizing that material flow varies across multiple areas of the weld; therefore, different elements of the FSW processes influence the texture within these zones. The impact of these texture variations on properties like yield strength is significant, where a material's yield strength heavily impacts residual stress generation. This impact will be explored in greater detail in section “4.1.5 Texture Measurements of Single Welded Plates”.

Figure 101 shows that the advancing side consistently experiences more plastic strain. However, this does not always translate to higher residual stress on the advancing side. For instance, while Plates B and D show a significant difference in residual stress between the advancing and retreating sides, Plates A and C do not exhibit this disparity (see Figure 97). In fact, Plate C has almost identical residual stresses on both sides despite the higher plastic deformation on the advancing side. This inconsistency might be attributed to the complex interplay of several factors, including material flow, thermal gradients, and misfits. While, in general, it's been observed that overall residual stresses increase with welding speed, the relationship is not straightforward due to these interacting factors. For example, the thermal gradients for Plate C are not as pronounced as those for Plate B and D despite the higher plastic strain on its advancing side (see Figure 94). The asymmetric temperature gradients and resulting strain suggest that a combination of thermal gradients and the resulting plastic strain or misfit could influence the disparity in residual stresses between the advancing and retreating sides. The intricate interplay between these factors likely defines the final state of residual stresses in the FSW process.

Understanding the significance of these findings is crucial for optimizing welding parameters and improving the structural integrity of welded joints. The delineation of stress profiles and their correlation with welding speeds, thermal gradients, and plastic strain distributions offers invaluable insights into the mechanics of FSW. This knowledge not only aids in predicting the behavior of welded materials under operational stresses but also in devising strategies to mitigate potential weaknesses in welded structures.

As the discussion now pivots to texture in the subsequent section, the relevance of texture becomes increasingly apparent in light of these observations. Texture, or the orientation distribution of grains within a material, plays a pivotal role in determining the material's mechanical properties, including its response to stress and strain. Given the significant impact of welding parameters on plastic strain distribution and residual stresses, understanding how these parameters influence texture development during the welding process is paramount. The alteration of material texture can significantly affect the mechanical behavior of the weld zone, potentially influencing the overall performance and durability of the welded structure. Hence, delving into the study of texture is a logical progression from examining residual stresses, as it provides a deeper understanding of the microstructural changes that underpin the observed mechanical responses.

4.1.5 Texture Measurements of Single Welded Plates

Texture refers to the orientation distribution of grains in polycrystalline materials. It affects material properties such as Young's modulus, yield strength, ductility, and thermal expansion. These properties are anisotropic in materials with strong textures, meaning their values vary depending on the direction in which they are measured. This anisotropy directly influences how the material responds to mechanical and thermal loads, which are critical factors in the development of residual stresses. FSW involves severe plastic deformation and thermal cycles, leading to significant microstructural transformations in the weld zone and adjacent areas. The intense localized plastic deformation promotes the development of new textures, which can differ substantially from the base material. Since mechanical properties like yield strength and ductility are affected by texture, the redistribution of stresses during welding and cooling will also be influenced, impacting the magnitude and distribution of residual stresses. Numerous studies highlight not just the emergence of new textures but also significant alterations in texture across the weld zone. For instance, Moradi et al. [146] discovered that the textures developing on the retreating side of the weld differ significantly from those on the advancing side. They found that, in contrast to the advancing side, the retreating side exhibits a texture strength that surpasses that of the original AA6061 sheet. The variations in texture, both within and across the weld, and the reasons behind their formation have been explored by other researchers; for example, Bajaj et al. [208] describe a marked decline in temperature with increasing distance from the pin's surface which causes the thermal gradient to increase the flow stress further away from the pin. Bajaj et

al. [26] continue to explain that the flow stress is primarily influenced by temperature in metals, with a lesser dependence on shear rate. As a result, the flow stress increases as one moves radially outward from the pin, causing most deformation to occur within a confined area close to the pin. Additionally, the stark contrast between areas of intense recrystallization and those with minimal deformation suggests that severe plastic deformation is concentrated within certain zones. This zone acts as a boundary between the stationary base material on one side and the deformed, flowing material on the other, effectively serving as a shear zone where the material is subject to intense plastic deformation. Furthermore, the behavior of the material at the center and directly ahead of the tool is governed by a distinct process. Bajaj et al. [208] explain that while the material in front of the tool is repositioned to a spot behind its original location in relation to the direction of welding, it is never displaced by more than the diameter of one pin. As a result, there are various zones, mechanisms, and deposition methods to take into account, each contributing to the formation of diverse textures.

Microtexture analysis, often conducted through techniques like Electron Backscatter Diffraction (EBSD), provides detailed insights into the orientations of individual grains and their spatial distribution. This level of detail is important for understanding the localized effects of FSW on the material's microstructure (see Figure 102). Analyzing how the crystallographic orientations change within the weld zone provides a more complete understanding of the residual stress distributions. Understanding the relationship between texture, anisotropy, and residual stresses can lead to better control over the FSW process and optimization of weld properties.

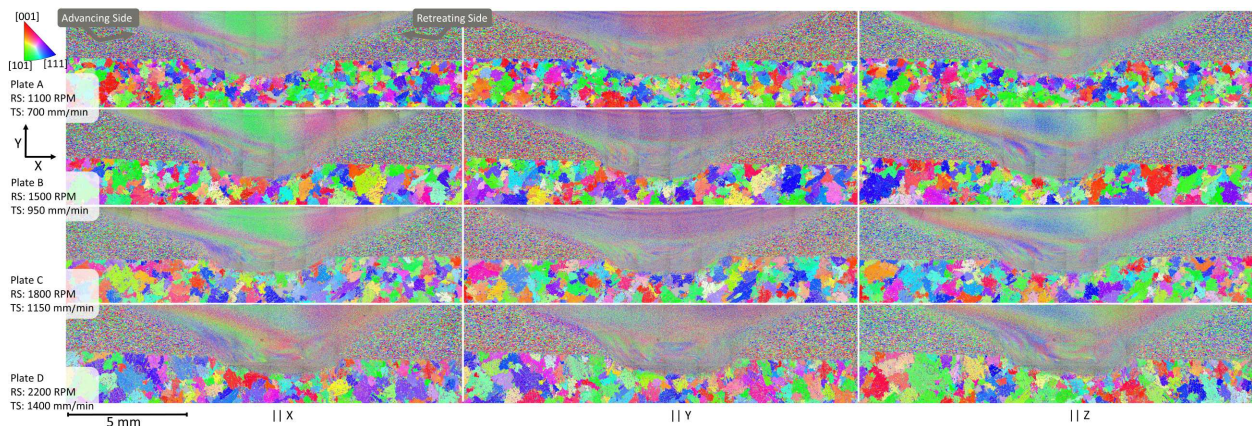


Figure 102: Inverse Pole Figure (IPF) EBSD maps

Inverse Pole Figure (IPF) maps visually represent the crystallographic orientation of materials in relation to a fixed reference frame (e.g., the sample surface). Unlike Euler angle maps, which represent orientation through three angles defining a rotation from a reference frame to the crystal frame, IPF maps directly relate the crystal orientation to a sample direction. IPF maps offer a more intuitive understanding of material orientation by directly correlating crystal directions with the sample reference frame. IPF maps use color coding to represent different crystal directions relative to the sample reference frame, making it easier to visually identify and distinguish between different orientations. IPF maps allow for easy identification of specific orientations, such as those aligned with the loading direction in mechanical testing. This capability is particularly beneficial in studies focused on texture and anisotropy, where understanding the orientation distribution relative to external conditions is important.

Figure 103 presents a 3D representation of the surface texture, where the top weld surface showcases the "onion rings" formation that is commonly observed in many friction stir welds. According to Krishnan et al. [213], this onion ring pattern results from a geometric effect as the material is extruded with each rotation of the tool. The slicing through the material section creates visible rings, with the spacing between them attributed to brief pauses in the tool's movement, allowing time to generate frictional heat and extrude material cylindrically around to the retreating side of the joint. The spacing of these markings corresponds to the tool's forward motion during one rotation (Krishnan et al. [213]).

Additionally, Figure 103 highlights the consistency of this texture from the front surface deep into the material. This consistent textural pattern is crucial for the analysis in subsequent images used for pole figures and ODFs (refer to Figure 105 and Figure 106) across a specific area of through-thickness. Such consistency bolsters confidence in the findings related to pole figures and ODFs, demonstrating that the texture from the front extends uniformly through the welding direction and depth from the front surface forward. Moreover, the IPF map reveals a distinct long-range texture specific to FSW. While studies have explored textures resulting from various forms of plastic deformation, such as forging, rolling, and extrusion, the texture resulting from FSW—though less frequently documented, especially in aluminum—deserves recognition as a unique category. This study emphasizes the need to consider FSW texture as its own specific type, much like the well-established forging, rolling, and extrusion textures. Recognizing FSW texture as a distinct category

opens new avenues of research, highlighting its influence on mechanical properties and other characteristics of the weld. Regardless, the advantages of the IPF map are especially useful in identifying differences between the advancing and retreating sides in FSW; for example, in Figure 102, when focusing on the IPF map parallel to the X direction of the map, distinct orientation differences are seen between the advancing and retreating sides compared to the center of the weld. These differences are consistent across all welding parameters.

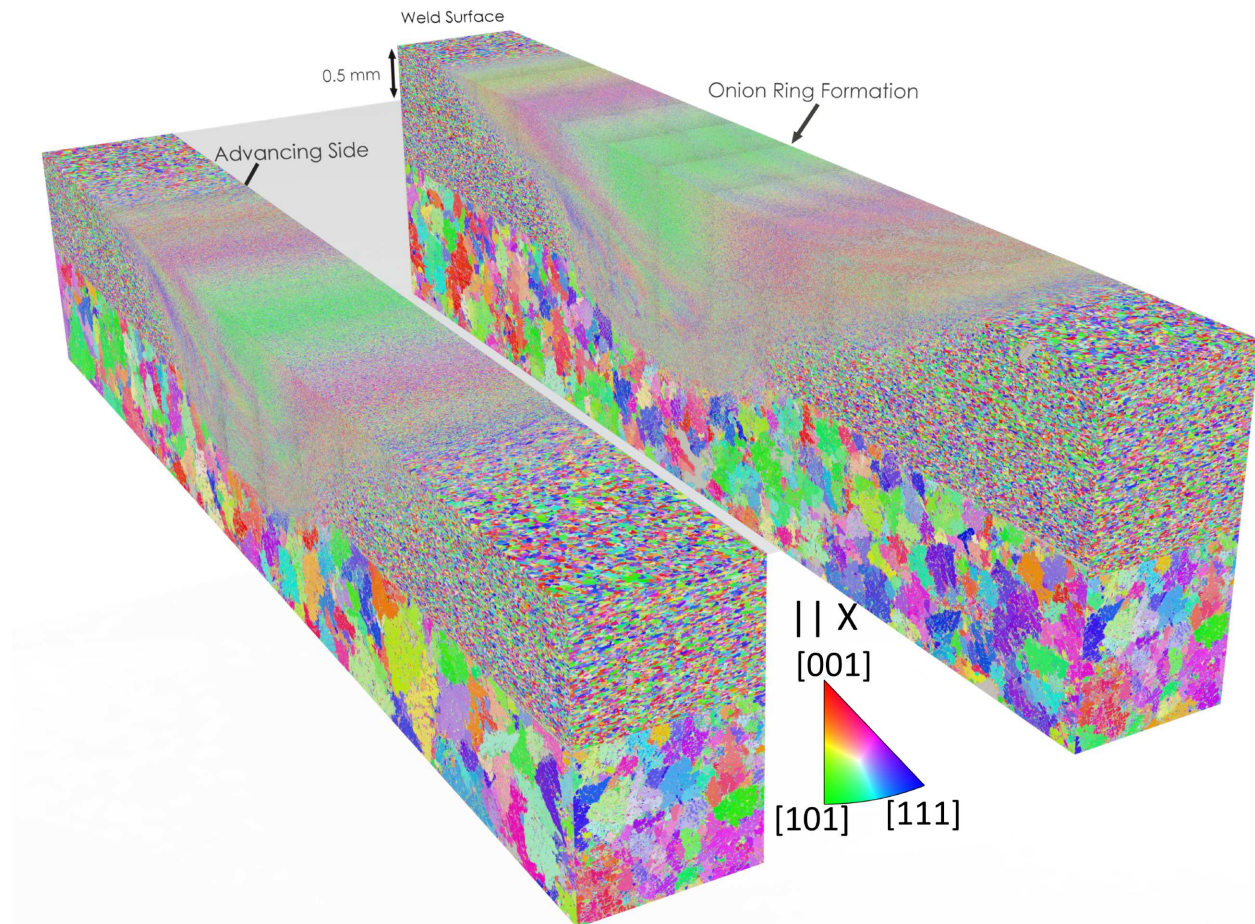


Figure 103: 3D Inverse Pole Figure (IPF) map of friction stir welded aluminum alloys

By mapping the orientation of grains across the weld, insights can be gained into the material's microstructure and, by extension, its mechanical properties, such as yield strength. This correlation is possible because the crystallographic orientation of grains affects the material's behavior under stress. For instance, certain textures can indicate a material's tendency towards anisotropic mechanical properties, where the strength varies based on the direction of applied stress. These stresses can be from non-uniform thermal gradients after the tool has already created a certain texture in the material. As this texture significantly affects the yield strength, it will also

significantly affect how the residual stress forms. For example, it is known that residual stress cannot be greater than the yield strength as strains greater than the yield strain produce plastic deformation rather than residual stresses. Therefore, when the material cools down, the yield strength increases and the residual stresses are formed, but this is limited by the temporary yield strength defined by the temperature. Like temperature, texture also affects the yield strength, but this is no longer transitory; it will permanently affect the final yield strength, thereby limiting what type of residual stress profiles formed. Yield strength changing across the weld dependent on the tool location has been documented by Lemmen et al. [58] where this study found that the pin location had a much different yield strength compared to the shoulder region where, in many cases, just outside the shoulder region had a much higher yield strength for many of the aluminum alloys tested compared to the yield strength in the center pin location.

At the beginning of this section, researchers like Bajaj et al. [26] and Field et al. [32] highlighted that there are significant variations in the crystallographic texture across the thickness and width of friction stir welds. Considering this, if the texture in the center is oriented in a way that alters the anisotropic response of the material's yield strength—upon which the residual stress response depends—then this texture variation is likely responsible for the observed changes in yield strength across the weld. This is because the dependency of yield strength on temperature no longer applies after the weld has cooled. Raddle et al. [214] and ISO/TS 21432:2005 [123] standard have emphasized that grains' crystallographic texture or orientation significantly influences various material properties, including Young's modulus, yield strength, ductility, and thermal expansion, potentially altering these properties by 20-50% of their original values. Reinforcing the previously mentioned findings, and perhaps even more notable, is the observation that texture intensity decreases as welding speed increases. This phenomenon can be identified by isolating the dynamically recrystallized zone, utilizing the boundary marked by misorientation in the thermomechanically affected zone, as illustrated in Figure 104.

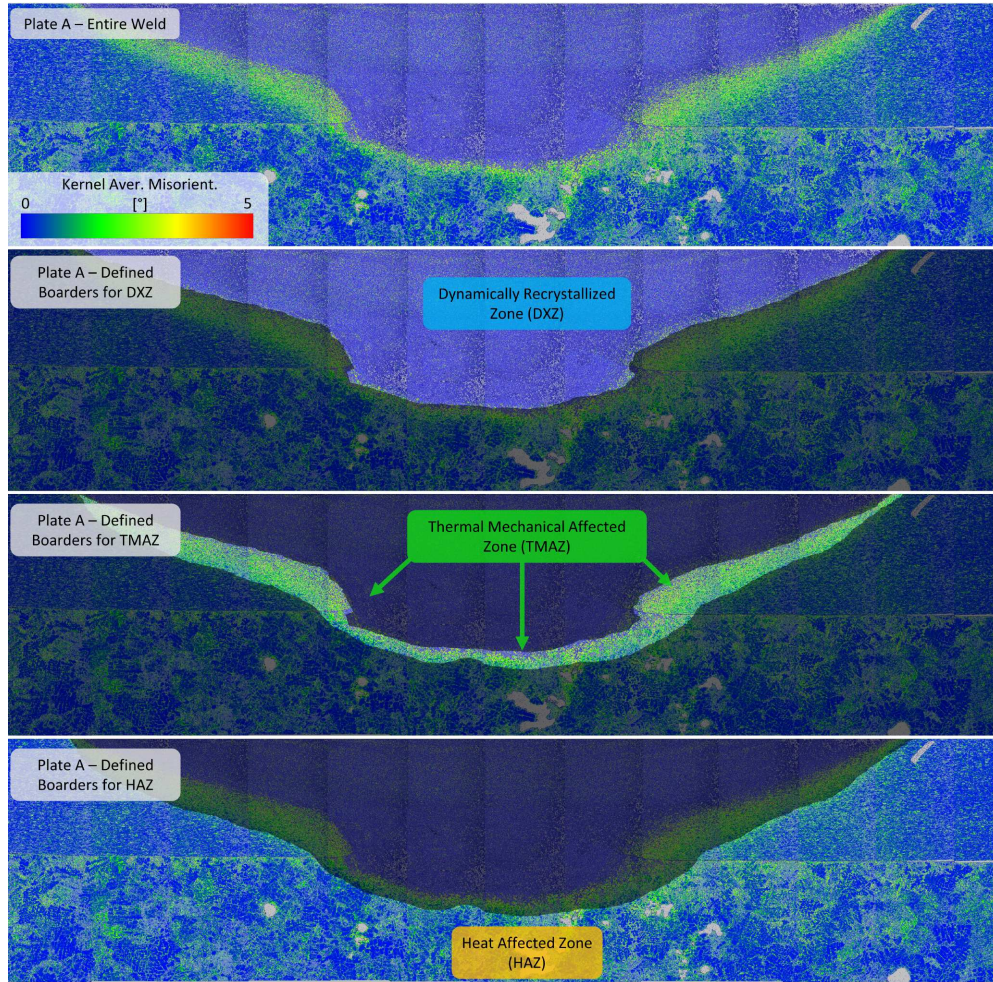


Figure 104: Kernel Average Misorientation (KAM) of friction stir welded aluminum alloys

To clarify, Figure 104 does not show a decrease in texture intensity. Instead, it indicates the boundaries of the dynamically recrystallized zone. Defining these boundaries and isolating this zone from the rest of the EBSD map allows for a focused analysis of the grains that are recrystallized due to the FSW process, preventing the dilution of the pole figures and subsequent ODF maps with non-welded material. Therefore, Figure 104 is presented to explain the regions within the weld and how the kernel average misorientation map is used to define the dynamically recrystallized zone accurately. For a clear observation of the phenomenon where texture intensity decreases with increasing weld speed, refer to Figure 105, which showcases a pole figure where the overall intensity is reduced from plate A to plate D (increasing welding speeds). Additionally, the ODF sections (see Figure 107) are calculated again for plates A to D, further demonstrating that the texture decreases with increasing welding speed.

Nevertheless, by isolating the DXZ and creating several pole figures of this region for each of the welded plates, interesting texture responses can be identified, as shown in Figure 105. The pole figure results show that the intensity of the texture tends to diminish with increasing welding speed. Higher welding speeds lead to higher temperatures (see Figure 94) and lower torques (see Figure 90), which promote dynamic recrystallization. Dynamic recrystallization can lead to a more randomized crystallographic orientation, thus reducing the texture (see Figure 105). The reason reducing texture increases material properties could be due to the negation of texture-induced grain coarsening. The ever-increasing rotational speed of the tool can cause significantly less ordered texture throughout the weld (i.e., a more random FSW texture), affecting yield strength. One might be tempted to attribute the differences in mechanical properties to grain size refinement. However, as concluded in “3.2.3 EBSD”, the grain size was almost consistent between the two very different welding parameters on the battery tray due to a consistent speed ratio. This consistency was also found in each of the welds on the single weld plate. The equivalent circular diameters (ECD) of the grains detected by EBSD in microns for the dynamically recrystallized zone (DXZ) for each plate are as follows: plate A was 18.26 μm , plate B was 17.02 μm , plate C was 16.88 μm , and plate D was 16.78 μm . These diameters show that the ECDs of the grains within the DXZ are essentially the same and very close to those recorded on the battery tray. The minor differences, ranging between 13 and 17 microns, are likely attributed to potential variations in the initial grain size of the 6061 plate material or the fact that the grain measurements for the battery tray were taken from the top surface, while the measurements for the plates were taken from the cross-sectional weld surface. Regardless, a three-micron difference between these weld groups is surprisingly low, lending further support to the conclusion that, with a constant speed ratio, the grain size remains consistent regardless of the specific welding parameters (rotational and traversing speeds) used or at the very least, it will not change drastically.

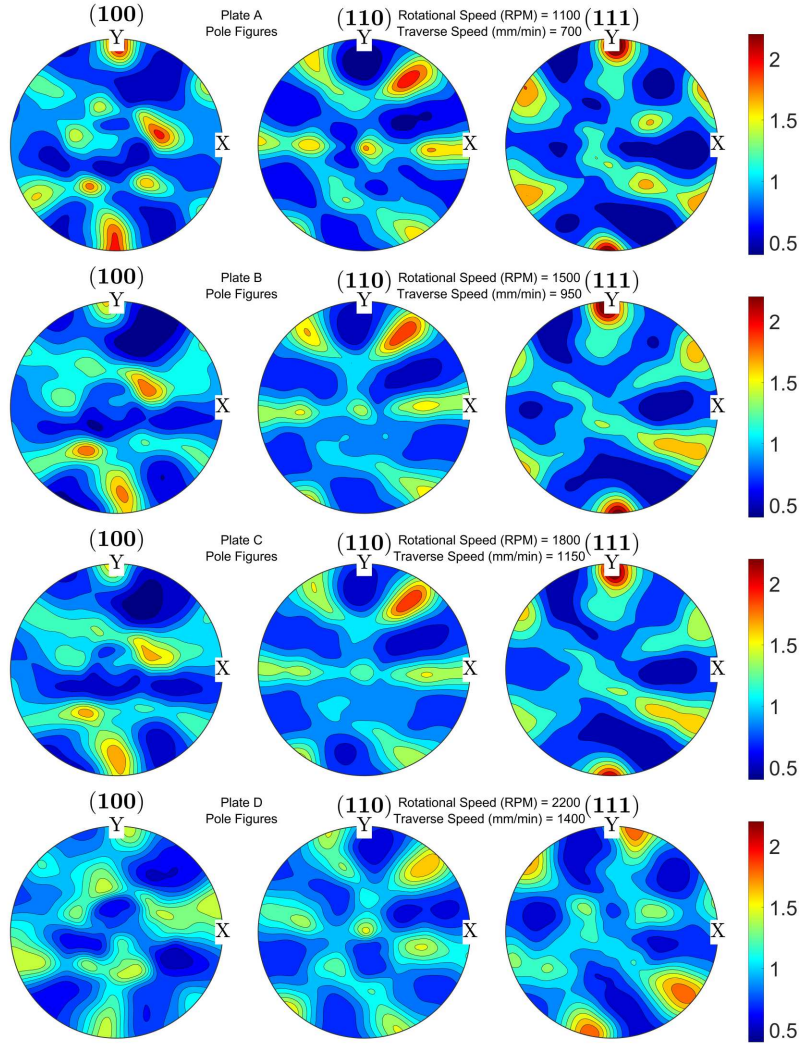


Figure 105: Pole figures of the isolated DXZ for Plates A to D

To support the concept of reducing texture and increasing randomness with increasing welding speeds, an Orientation Density Function (ODF) for each weld (see Figure 107) is extracted from the previously defined DXZ zone. Important to ODFs are the Euler angles (ϕ_1 , Φ , and ϕ_2), which are used in texture analysis to represent the orientation of a crystal lattice in a material. These angles define the rotation of the crystal lattice in three-dimensional space. In texture analysis, the Euler angles are used to create the ODF, a statistical representation of the distribution of crystallographic orientations in a polycrystalline material. The entire ODF can be plotted as many sections or is often represented in sections at constant ϕ_2 , known as ϕ_2 sections. Specific orientations within the ϕ_2 sections can be assigned to a $\{hkl\}\langle uvw \rangle$, which represents an orientation of a crystal lattice, where $\{hkl\}$ represents a family of planes and $\langle uvw \rangle$ represents a

family of directions within those planes. In combination with ϕ 2 sections, the $\{hkl\}\langle uvw \rangle$ can be utilized to identify which textures are dominant in the friction stir weld (see Figure 106).

Many of the lower index planes such as $\{100\}$, $\{110\}$, and $\{111\}$ and directions such as $\langle 100 \rangle$, $\langle 110 \rangle$, and $\langle 111 \rangle$ are typical in cubic systems like the face-centered cubic (FCC) structure of aluminum and are often measured in such processes as cold rolling and hot rolling, where specific texture types occur due to recrystallization, rolling, or shearing (Zhang et al. [215]). Zhang et al. [215] provide experimental results of the anisotropic differences between different directions of the rolled material and outlined how, with increasing deformation, these textures act differently depending on what caused them. For example, the anisotropy in elongation for the stress vs strain curve was affected by recrystallization textures and shear textures, but the yield strength anisotropy was significantly affected by rolling textures. Given FSW causes recrystallization textures, shear textures, and rolling textures (Emami et al. [216]), it can be akin to a multi-modal rolling process of cold rolling and hot rolling with extreme amounts of deformation where the amount of recrystallization, shear, and rolling textures change across the weld as shown in Figure 106. The colors on the ODF maps in Figure 106 are used to indicate how closely the local crystal orientation in the material corresponds to certain "aligned orientations." Aligned orientations refer to specific planes and directions that are of interest. In practical approaches, due to the fact real-world materials are not in perfect alignment down to the exact degree, the assignment includes a tolerance of 30° . This tolerance means that any crystal orientation within 30 degrees of a specified ideal orientation is still considered to be aligned or nearly aligned to that orientation. If points satisfy conditions for multiple textures, the color representing the closest match is utilized. This method allows for a distinct visual differentiation of grains according to their texture components. Analyzing Figure 106, the $\{100\}\langle 110 \rangle$ texture found in the center of the weld bead is a type of shear texture that is commonly observed in FCC materials like aluminum (Lyu et al. [217]). Moreover, this specific $\{100\}\langle 110 \rangle$ texture is shown to be induced by heightened exposures to temperature (Lyu et al. [217]). It is interesting to note that the center surface of the weld is the exact location of the maximum temperature on the trailing edge of the FSW tool, as shown in Figure 93. Additionally, texture-induced grain coarsening could play an important role. While direction and loading are important, the presence of hundreds to thousands of highly aligned grains can also impact the yield strength. These factors were previously discussed in the section "2.2.5.1 Texture Considerations" in the literature review.

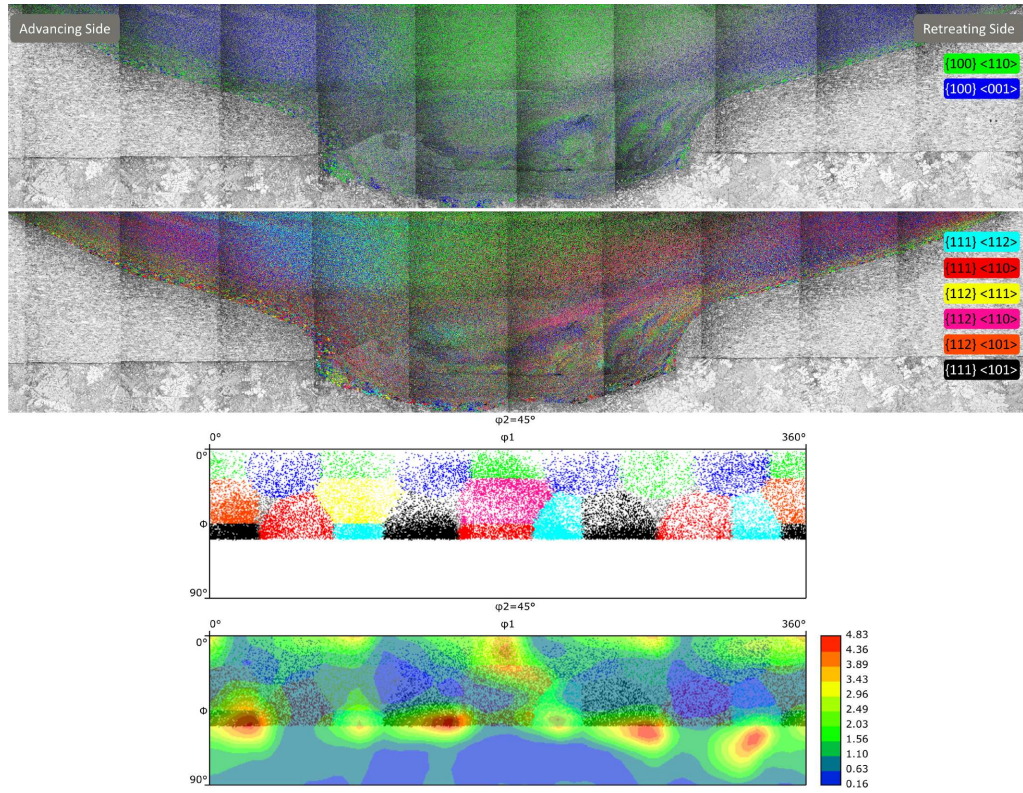


Figure 106: ODF section and texture isolation of friction stir welded aluminum alloys

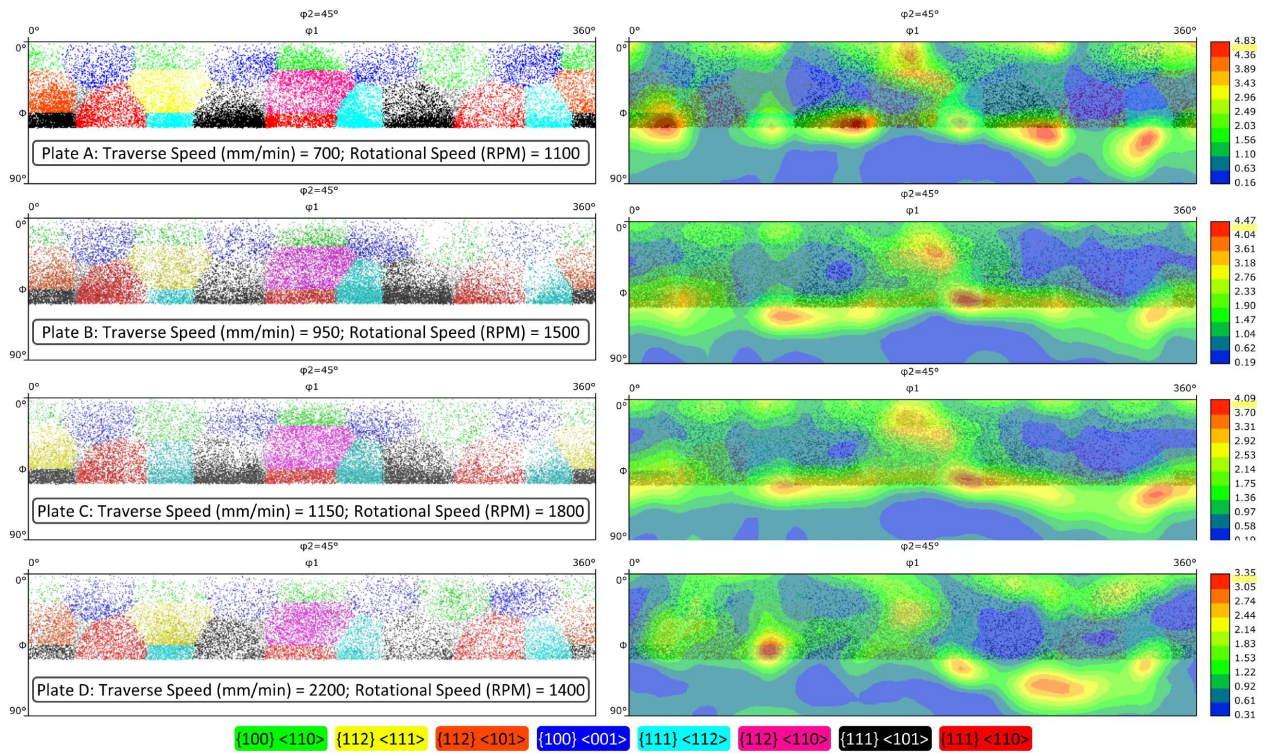


Figure 107: ODF sections for Plates A to D

Similar findings are presented by Woo et al. [218]. Although the study focuses on Magnesium, it provides important insights relevant to the Aluminum system. Changes in texture, specifically the basal planes in the stir zone, were observed to increase the Schmid factor, leading to enhanced elongation and decreased tensile strength. The tensile behavior of the longitudinal specimen, mainly the stir zone, showed a significant increase in elongation and a decrease in tensile strength compared to the base material. A subsequent study by Woo et al. [62] directly linked the texture-induced reduction in yield strength to the primary cause of residual stress reduction in the center of the weld or the trough of the "M" shaped profile, aligning with the discussions in the present study. Fortunately, the Schmid factor map can be calculated from EBSD data (see Figure 108). As seen in Figure 108, large areas of the DXZ contain many grains with high Schmid factors. One of the largest regions happens to be on the advancing side of a friction stir weld, potentially corresponding to the area identified with maximum strain in Figure 101, near the pin on the advancing side. The shoulders of the region in Figure 108 have less concentrated areas of high Schmid factor grains. This concentration could lead to significant localized plastic deformation, making this area a potential weak point in the weld and reducing its ability to retain residual stress.

Interestingly, in the precise region where high tensile residual stresses are found (on the interface between the TMAZ and HAZ), there are no large-scale aligned grains with similar Schmid factors. In materials where high and low Schmid factor grains are interlaced (as seen in the base 6061 material in Figure 108), the low Schmid factor grains can hinder the slip in high Schmid factor grains. This barrier effect can reduce the ability of residual stress to release into plastic deformation, leading to higher residual stresses.

Ultimately, it seems that small grains with similar high Schmid factors can collectively facilitate mass slippage, whereas a heterogeneous mix of grain orientations (high and low Schmid factors) can enhance the material's strength by resisting deformation. This observation aligns with the yield strength results from the Profilometry-based Indentation Plastometry (PIP) discussed in the next section (see Figure 118). The PIP results indicate that the lowest yield strength is measured directly in the DXZ, but past the end of the TMAZ and into the HAZ, the highest yield strength is found.

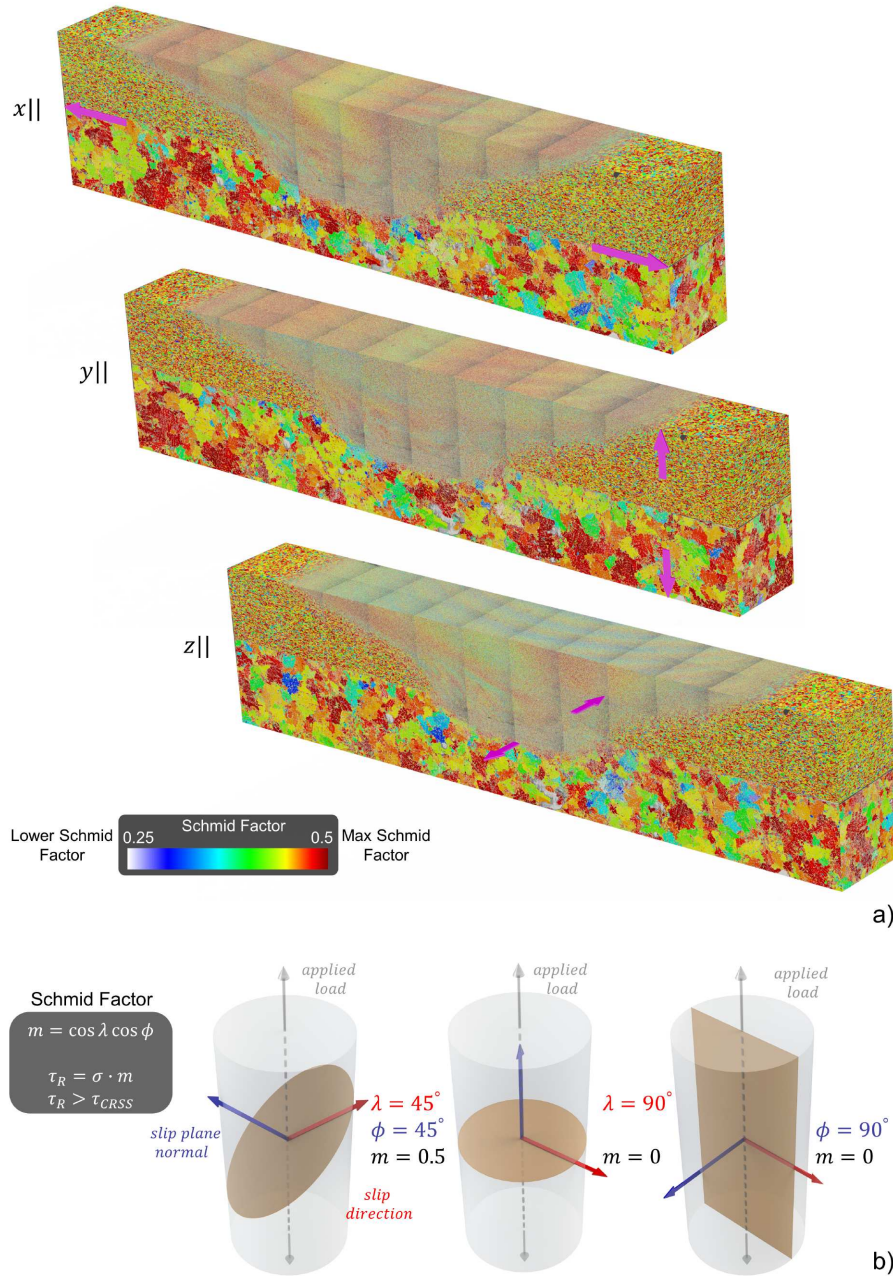


Figure 108: (a) Distribution of Schmid factor grains in friction stir welded aluminum material loaded in three different directions; (b) Alignment of high and low Schmid factor planes/grains to the applied load (0.5 maximum Schmid factor)

4.1.6 Section Conclusions

Using the widely studied and well-established 6061 and A365 Al system, the effect of four specific welding parameters with consistently increasing the rotational and traverse speed at a consistent speed ratio of 1.57, including the spindle torque data, temperature simulations, residual stress, and texture in a wrought-to-cast lap FSW has been demonstrated and characterized using neutron

diffraction, EBSD, and SPH. Within the range of the materials and welding conditions used, the conclusions are as follows:

- (1) The torque required for welding decreases as the speed of the FSW tool increases while maintaining a constant speed ratio. This trend is observed as the speed incrementally rises from Plate A, with settings at 700 mm/min and 1100 RPM, to Plate B at 950 mm/min and 1500 RPM, Plate C at 1150 mm/min and 1800 RPM, and finally to Plate D at 1400 mm/min and 2200 RPM.
- (2) With increasing welding speeds, the maximum temperature achieved during welding rises for plates B, C, and D, with temperatures of 490°C for plate B, 515°C for plate C, and 550°C for plate D. Additionally, thermal exposure diminishes with faster welding speeds, resulting in less total thermal exposure due to increased traversing speed.
- (3) Stress-free measurements vary with different welding speeds, but these variations do not follow a consistent pattern and are specific to the speeds applied. The main differences are associated with the stirring and rotating actions of the pin, which create distinct zones from the weld's center to its edges. This effect is thought to result from the segregation of particular elements within the alloy, leading to changes in stress-free reference values. Consequently, this creates a requirement for multiple stress-free measurements in diffraction-based studies to accurately determine residual stress levels.
- (4) Longitudinal stresses escalate with increased welding speeds, ranging from a minimum peak stress of 170 MPa for plate A to a maximum peak stress of 230 MPa for plate D. This trend is supported by SPH simulation temperature gradients, showing plate D with the most intense thermal gradients and the most significant residual stress reactions.
- (5) SPH simulations show that maximum plastic strain is predominantly observed on the advancing side of the tool pin, almost doubling the strain compared to the retreating side. The peak strain is located in the pin region rather than the tool's shoulder area.
- (6) Across the center of the weld on Plates A, B, C, and D, normal and transverse stresses are minimal in comparison to longitudinal stresses. However, as measured for Plate A, steep thermal gradients significantly influence transverse stresses, starting with an average in-weld stress of 73 MPa at the beginning, decreasing to 32 MPa in the center, and then rising to 103 MPa at the weld's end. The maximum stresses in the transverse direction, along with

the peak stresses in the longitudinal direction of 190 MPa at the weld's end, might account for the frequent instances of failure observed near the weld's exit.

- (7) As welding speeds increase, there is a noticeable reduction in maximum texture intensity, decreasing from 4.83 for Plate A to 4.47 for Plate B, then to 4.09 for Plate C, and finally to 3.35 for Plate D. Despite these changes, the average grain size within each plate's weld zone (DXZ) remains consistent, measuring within a few microns of 17 μm due to the constant speed ratio utilized. The faster welding speeds and reduced texture may diminish the anisotropic impact, potentially through the mechanism of texture-induced grain coarsening, on yield strength. This reduction in texture allows for higher and/or altered yield strength values in the weld material from Plate A to Plate D, which may affect the residual stress response that is closely linked to the material's yield strength. Therefore, texture may indirectly impact residual stress by influencing yield strength.
- (8) The distribution of Schmid factors within the friction stir weld seems to significantly contribute to the influence of mechanical properties (i.e., the yield strength). Regions with high Schmid factors, particularly by the near tool pin advancing side, may facilitate localized plastic deformation and reduce the ability to retain residual stress. Conversely, areas with interlaced high and low Schmid factor grains hinder slip, potentially contributing to the build-up of tensile residual stresses.

Having analyzed the specifics of each single weld plate and the varying welding parameters at a constant speed ratio, a clearer understanding of the direction in which residual stress tends to generate with increasing weld speeds has been obtained. This illuminates' certain aspects of the battery tray welding process. If the welds were conducted independently of each other, the center weld or the non-peripheral weld on the battery tray would have generated much more residual stress. However, this was not the observed case, and the results from this section now confirm this. Increasing welding parameters, such as rotational and traversing speed, does indeed increase residual stresses. However, on the battery tray, it was theorized that the order of operations played a crucial role. The center weld, which was conducted at 1.5 times the speed of the peripheral weld, was applied first, allowing its residual stresses to be relaxed by the subsequent welds. With the confirmation that residual stresses increase with higher speed parameters, it is now an optimal time to delve into the proximity welding portion of this dissertation. The next section will explore the

effects of the proximity of multiple welds on residual stress and observed distortion, connecting the findings from the in-house single-weld plate study to the observations made on the battery tray.

4.2 Influence of Multiple Welds on Residual Stress and Distortion in Friction Stir Welding: A Neutron Diffraction Study

Examining the sequence and order of welds as a function of residual stress generation is an important area for optimizing FSW processes and improving the performance and reliability of welded components in various industrial applications, yet few studies have explored this area. In FSW, heat generated by the rotating tool softens the material, which then reforms behind the tool as it advances. This process can cause residual stresses to form in the material due to thermal expansion and contraction, plastic deformation, and microstructural changes. The proximity and order of welds can affect residual stress in several ways. For example, if welds are too close together, they may overlap and create higher levels of residual stress in the material. Welding in a specific order may also affect residual stress, as current welds may interact with residual stresses generated by previous welds, potentially leading to changes in the residual stress profile.

Research on proximity welds and their effects on residual stresses has been more prevalent in fusion welding. Bhardwaj et al. [219] investigated residual stresses in proximity girth welds using fusion welding techniques, finding that close proximity welds can significantly increase tensile residual stresses due to high constraints and multiaxial stress states. This highlights the critical nature of weld sequence and spacing in fusion welding. However, such extensive studies and set standards for proximity welds are lacking in the context of friction stir welding. The unique characteristics of FSW, such as lower heat input and solid-state joining, necessitate specific data collection to understand how proximity and sequence of welds impact residual stresses in FSW.

Sun et al. [220] demonstrated that the number and distribution of weld seams critically influence the forming accuracy and mechanical properties of spun-formed workpieces, highlighting the need for precise control over weld placement in complex components. Martin et al. [221] developed novel techniques for corner joints using FSW, emphasizing how different joint configurations and thermal cycles impact weld quality. Agapiou and Carlson [222] explored the optimization of welding parameters for copper components in electric motors, underscoring the challenges of managing thermal effects during welding. Notably, Agapiou and Carlson's work provides one of

the few examples of non-overlapping proximity welds and circularly enclosed welds in friction stir welding research despite being conducted on copper rather than aluminum. These aspects are both very important to industrial welding, highlighting the potential for cross-material insights and the critical need for specific studies focused on aluminum.

Residual stresses have significant effects on fatigue and distortion in welded components. Dalle Donne et al. [223] found that residual stresses are much more influential than weld quality in determining the fatigue crack propagation rates in friction stir welded joints, emphasizing the importance of understanding residual stress effects. Chen and Kovacevic [224] used finite element analysis to study stress evolution during FSW, demonstrating how residual stresses impact fatigue strength and distortion. Furthermore, Charitidis et al. [225] investigations using nanoindentation techniques demonstrate that residual stresses can lead to localized deformation and affect mechanical properties.

Therefore, a systematic investigation of the effects of welding sequence and proximity on residual stress in FSW can help optimize the welding process and minimize the detrimental effects of residual stress on the material. Hence, the present investigation aims to develop a comprehensive framework to determine the impact of welding process conditions relating to weld sequence on the formation of residual stresses in dissimilar aluminum alloy friction stir welds (i.e., 6061 joined to A365).

4.2.1.1 Weld Layout and Quality Assurance of Multi-Welded Plates

The present study utilizes a 6061 aluminum top plate welded to an A365 aluminum casting (see Table 1). The 6061 alloy dimensions were 400 mm (length) $\times$ 200 mm (width) $\times$ 2 mm (thickness), while the A365 alloy measured 400 mm (length) $\times$ 200 mm (width) $\times$ 7 mm (thickness), see Figure 109 for weld dimensions. This study uses constant welding parameters to compare welding sequences and order with an additional time variable for one of the plates. As shown in Table 2, the traverse and rotational speeds were set, while the plunging force was adjusted based on weld quality. Testing welds were utilized on additional plates to determine the optimal plunging force by gradually increasing it until defects ceased, as verified by ultrasound. The ideal plunging force of 6.9 kN was then applied to the plates studied. To ensure industry-quality welds, each plate was scanned using ultrasound, as shown in Figure 110 with Plate I.



Dwell Time (s)	Traverse Speed (mm/min)	Rotational Speed (RPM)	Plunge Force (kN)	Plunge Feed (mm/min)	Withdraw Feed (mm/min)
1.2	950	1500	6.9	120	250

The B-scan provides a longitudinal view of the depth, with colors corresponding to the A-scan amplitudes. The first 1 mm shows unreliable readings due to the dead zone, but below this, consistent green-yellow indicates low amplitude or near-zero readings, suggesting no significant defects. The B-scan also includes a transverse view, showing three cross-sections—two for the peripheral weld and one for the center—demonstrating good weld quality at the interfaces. The C-scan, included for completeness, outlines the top surface. Although it shows high and low amplitude readings, it is less reliable than the depth measurements due to the surface irregularities.

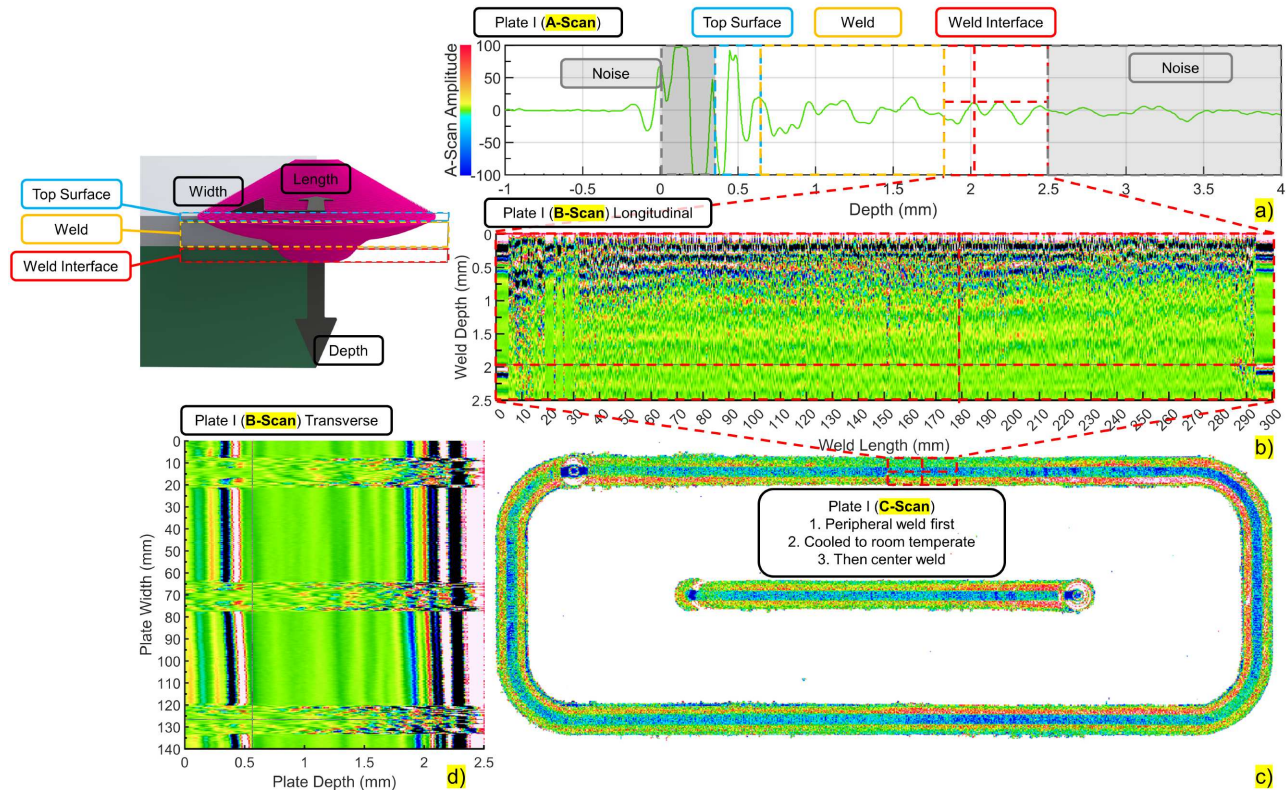


Figure 110: Ultrasonic non-destructive evaluation of friction stir welds; (a) one-dimensional display of amplitude versus depth; (b) cross-sectional view revealing defects across the length and depth; (c) external details of the weld projected onto a plane; (d) cross-sectional view revealing defects across the width and depth

The unreliability of depth readings on ultrasound in friction stir weld inspections is primarily due to surface irregularities and high-noise signals creating a dead zone. This dead zone affects the initial 1.0 mm from the surface, making it difficult to determine when and where the weld starts accurately. However, the method remains effective for detecting deeper defects, ensuring the overall integrity of the weld can still be assessed. Ultrasonic scans were utilized to validate the weld quality of each plate and weld in this study, following the same process described in Figure 110. The final parameter requiring consideration is consistent fixturing, which is important for comparability between plates. The manner in which material is constrained during welding and released post-welding can influence distortion and residual stress. For example, Costa et al. [193] demonstrated that changing the releasing strategy from counterclockwise (CCW) to clockwise (CW) affects residual stress and plate distortion, albeit with minor effects. Therefore, the plates in this study maintained uniformity in clamping configurations and release schedules, as shown in Figure 111.

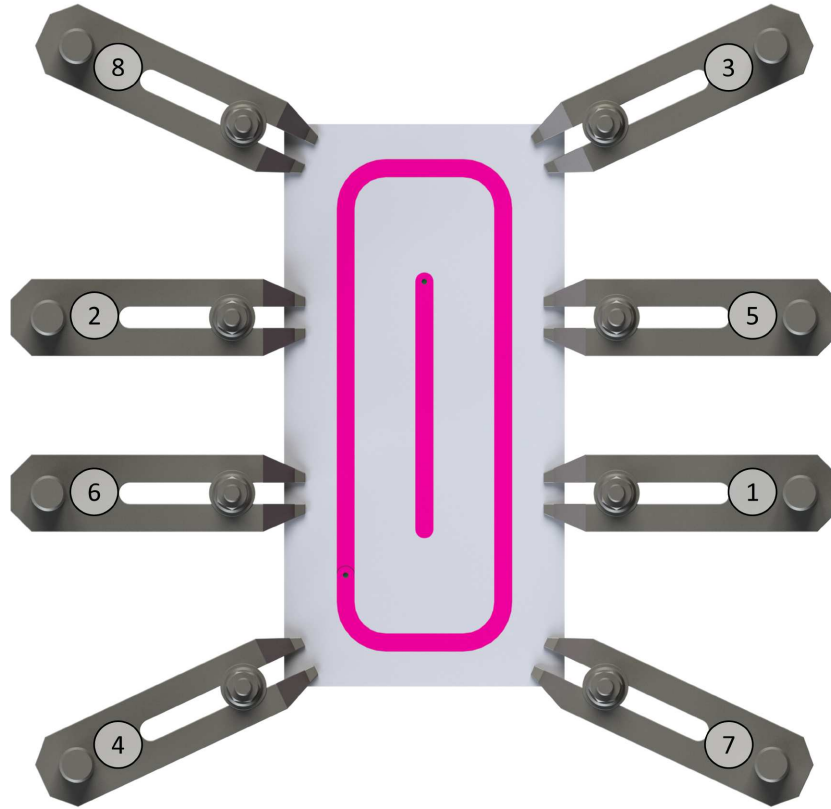


Figure 111: Rigging and releasing conditions utilized (1 start, 8 end)

4.2.1.2 Coordinate Measuring Machine

The plates were scanned for distortion post-friction stir welding using a coordinate-measuring machine (CMM) system with laser feedback technology for high-precision 3D scanning and measurement. The scanner employs SHINE (Systematic High-Intelligence Noise Elimination) technology to ensure high-quality data collection on surfaces with varying reflectivity, such as the glossy material of plate 6061. A wide laser scan line covered the entire surface area of each plate, and the system's high point acquisition rate—up to 1.2 million points per second—ensured the creation of detailed and accurate 3D models for comparison to a non-distorted 3D model.

4.2.1.3 Profilometry-based Indentation Plastometry

Profilometry-based Indentation Plastometry (PiP) is an advanced technique used to obtain detailed yield strength results in the present study. This method is akin to hardness while achieving outcomes typically expected from tensile testing, particularly for yield strength. PiP involves higher loads than traditional hardness tests and collects extensive data in the form of load-displacement plots or residual indent profiles.

The experimental process of PiP utilizes a spherical indenter, typically made of durable materials such as WC-based cermet, which creates an indentation in the material. The shape and size of the residual indent, or the load-displacement data, are then analyzed to extract a true stress-strain relationship. This relationship can predict the nominal stress-strain curve of a conventional tensile test, including ultimate tensile strength and post-necking characteristics. This process is enhanced by iterative finite element method simulations of the indentation process, which are integrated into commercial testing, making the technique reliable.

The primary advantage of PiP is its ability to measure bulk properties from small, flat samples. The volume of material being deformed must be representative of the bulk material, typically requiring the deformation of a large quantity of grains. This ensures the influence of crystallographic texture and grain boundary interactions are captured. Consequently, PiP provides a comprehensive understanding of the material's plastic deformation behavior, enabling accurate yield strength measurements (Clyne et al. [226]). Internal testing by Kozakevich et al. [227] found that yield strength values between multiple alloys are very similar (see Table 5). While ultimate tensile strength shows large variability between PiP and ASTM standard tensile testing, yield strength remains accurate within a numerical range of the uncertainty of 3-5%. Thus, the current data accurately reflects yield strength but not other values, which is sufficient for this study's purposes.

Table 5: PiP values compared to ASTM standard tensile testing (Kozakevich et al. [227])

Alloy	Method of Testing	0.2% YS (MPa)	UTS (MPa)	EL (%)
A356	PiP	103 ± 7.97	229 ± 3.56	20.83
	Tensile	100.25 ± 7.28	159.7 ± 25.17	2.87
B319	PiP	112 ± 1.53	298.5 ± 0.58	20.625
	Tensile	116.03 ± 0.12	152.9 ± 44.87	0.96
T4 A206	PiP	212 ± 39.34	419.67 ± 5.69	14.00
	Tensile	223.19 ± 0.035	342.5 ± 0.55	7.65

However, the limitations of PiP must be considered in this context. This is a non-ideal use case of PiP. As detailed in the following sections, PiP is utilized near the edge of a sample and sometimes within 6 mm of a previous PiP indentation. The results presented illustrate the differences in yield strength across the weld but should not be taken as absolute measurements of yield strength due to likely inaccuracies. The differences or trends observed are more valuable for understanding how various factors contribute to yield strength differences and resulting residual stress. Future

recommendations in this study will suggest methods better suited for this situation. The current results provide supporting evidence of yield strength differences, not absolute values.

4.2.2 Distortion

There are five plates labeled E, F, G, H, and I, each welded using friction stir welding (FSW) but with different configurations. Each weld on all plates has a uniform bead width of 13 mm. Plate E features a single central weld 175 mm long. Plate F includes a circular peripheral weld encircling the center weld, measuring 350 mm long and 125 mm wide (see Figure 109). Plate G incorporates a central weld followed by a peripheral weld, whereas Plate H is welded peripherally first and then centrally. Plate I differs in that after the peripheral weld, it is cooled to ambient temperature before the central weld is applied (see Figure 112 for weld order). Distortion measurements were taken using a coordinate measuring machine (CMM) from a flat cad reference model. Distortion on a 400 mm long plate ranges from +1 mm to -1 mm depending on weld configuration, indicating noticeable changes considering the component's thickness (see Figure 109). While joint configuration plays a significant role, the exact degree of distortion depends on various factors, including the specific welding parameters, fixture setup, and the welded materials. Therefore, making direct comparisons or drawing conclusions between different studies can be unrealistic due to these variables.

Specifically, Plate E shows negligible distortion at the start of the central weld, intensifying to -1 mm distortion toward only the end of the weld (see Figure 112). Plate F, which features only the peripheral weld and lacks a center weld (see Figure 112), displays notable distortion. Specifically, significant portions in the center of the plate show distortion at -1 mm, while the top left and bottom right corners exhibit distortions ranging from 0.75 mm to 1 mm, respectively. These differences in distortion are significant and can be attributed to the difference in bulk material support at the plate's edges and the considerably longer weld path of the peripheral weld compared to a configuration with only a center weld. Plate G, which completes the center weld first and then the peripheral weld directly after, exhibits significant distortion at -1 mm in the central areas covering large areas of the plate, with the top left and bottom right corners reaching up to +1 mm of distortion. The arrangement of the welds on Plate G, starting with the center followed by the peripheral weld, shows that the distortion affects nearly all four corners to about 0.75 mm, with

the bottom of the plate below the peripheral weld measuring distortions ranging from +0.25 to +0.5 mm across its width.

Following a pattern similar to Plate G, Plate H—which reverses the welding sequence by performing the peripheral weld first and then the center weld immediately afterward—exhibits positive distortion across the entire width of the plate below the peripheral weld. This similar distortion profile suggests that the welding sequence (whether peripheral or central first) may not significantly affect distortion, as the distortion magnitudes in the corners of Plates G and H are similar. However, Plate I's weld sequence, which allowed the material to cool down to ambient temperature between welds, shows the most considerable distortion, with significant curling at the corners and more extensive negative distortion across the center toward the top left corner of the plate. This distortion profile suggests that temperature plays a fundamental role in FSW; if a component cools between welds or is performed in separate stages, the distortion profile can vary significantly, even if the welding sequence remains the same. Most notably, considering the entire data set, single welds tend to cause minimal distortion, but adding multiple welds or allowing for cooling between welds increases distortion significantly. This observation will be further explored in subsequent parts of the study to understand the effects of interacting residual stress fields and temperature variations.

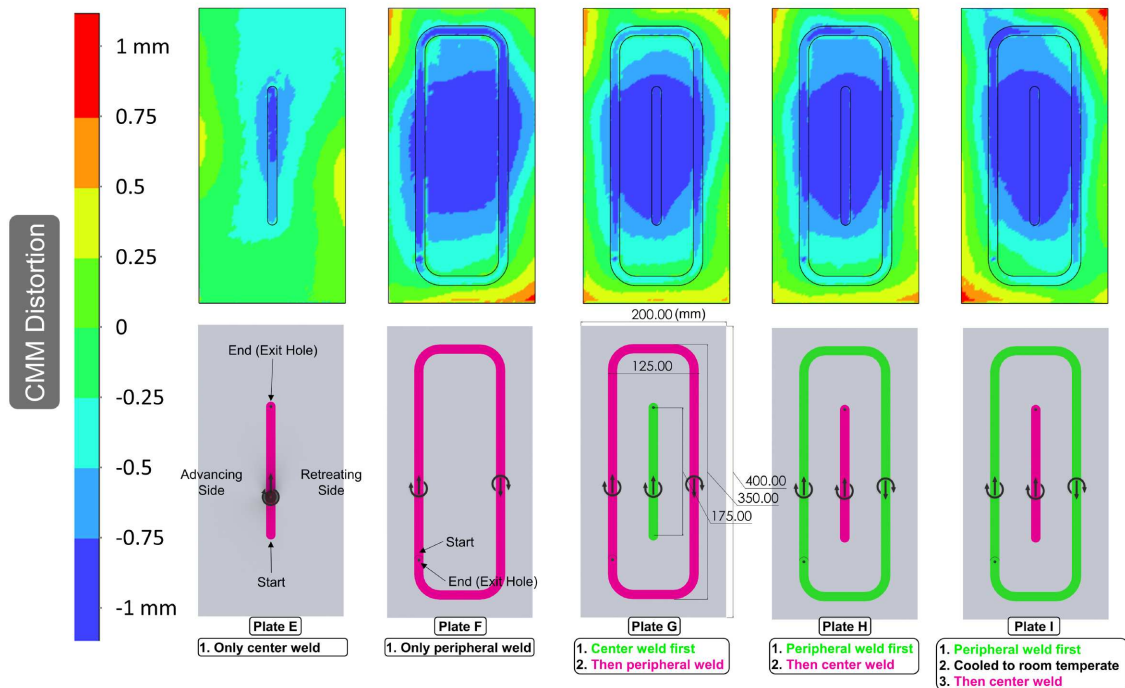


Figure 112: Coordinate measuring machine distortion (CMM) images of multi-welded plates

Distortion observed in the plates depicted in Figure 112 typically manifests as a combination of angular and bending distortions, mainly due to variations in the thermal field, material displacement, and longitudinal residual stresses throughout the plate's thickness. In thicker plates, achieving uniform conditions through the plate's thickness is increasingly difficult, a challenge often compounded by significant through-thickness temperature gradients. Xu et al. [228] identified these gradients as the primary drivers of residual stresses across the plate's thickness in FSW. Another form of distortion, buckling, results directly from the loads imposed by welding-induced residual stresses. As Williams et al. [82] remark, the applied weld load refers to the load generated by the residual stresses induced during the welding process. These residual stresses, particularly the compressive residual stresses in the outer regions of the welded plates, create a force or load on the structure. This load can cause various forms of distortion, including buckling if it exceeds the critical buckling load of the plates. The magnitude of this critical load is influenced by the stiffness and geometry of the plates, with thinner and narrower plates having a lower threshold for buckling (Williams et al. [82]). The specific buckling mode determines the final distortion's level and shape, which can often resemble bending distortion, making it easy to confuse the two. However, the origins and management strategies for buckling and bending distortions differ. Buckling direction can be highly unpredictable, influenced by variables such as the orientation of pre-existing residual stresses or the clamping configuration used during the welding process (Williams et al. [82]). The CMM images (see Figure 112) and the unpredictable nature of distortion underscore the importance of minimizing distortion through optimal welding practices. For example, rigging and clamping alter the resulting distortion and residual stress studied by Staron et al. [90] and Costa et al. [193]. Nonetheless, it is recognized that these practices alone cannot wholly eliminate distortion and residual stress due to the asymmetric nature of FSW (He et al. [229]). Addressing this challenge may require a standardized process to ensure a flat multi-welded component immediately after welding. This process would include measuring distortion and residual stress based on a specific clamping and rigging setup, then designing and implementing counter-rigging tailored to offset the anticipated distortion. This method would involve pressing the component against a custom-designed rig to neutralize distortion, ensuring the component remains flat post-FSW. Certain automotive companies have adopted a similar process to enhance throughput by minimizing manufacturing costs, as managing post-welding

distortion can be expensive and time-consuming. However, this process is not widely documented in literature due to its need for very specific rigging tailored to the welded component.

Nevertheless, various post-welding techniques can manage distortion if active management is unrealistic or too costly. Active distortion management refers to strategies implemented during the welding process to prevent or minimize distortion. This approach involves real-time adjustments and control measures to manage the residual stresses and deformation as they develop. The example mentioned above would be considered active management as it uses preemptive measures that involve anticipating potential distortions and implementing strategies to counteract them before they occur. Post-welding distortion management involves correcting any distortion that occurs after the welding process is complete. This approach focuses on remediation rather than prevention. For instance, the burnishing and coining post-welding techniques discussed in section “3.3.4 Residual Stress after Burnishing and Coining”.

4.2.3 Torque and Temperature Analysis

It's important to verify welding torques when examining weld sequencing in FSW, as torque is a key indicator of the load applied during the process and significantly influences welding quality (Shahi et al. [230]). In the current study, axial force and speed parameters were controlled, allowing torque to adjust freely. Monitoring the torque applied during the FSW process provides insight into the characteristics of the weld. Therefore, maintaining consistency in all aspects except sequence is essential; this ensures uniformity in heat generation, material softening, plasticizing, stirring action, penetration depth, and process stability across different plates. Figure 113 demonstrates that torque measurements are highly consistent regardless of the welding sequence.

This observation indicates that torque stability is maintained across different welding sequences, suggesting that effectively managing distortion may necessitate proactive and well-planned interventions rather than adjusting parameters such as torque during welding. Furthermore, torque appears unaffected by the plate's starting temperature during welding. This relationship and its implications can be explored further in subsequent analyses, particularly in relation to temperature, as shown in Figure 114.

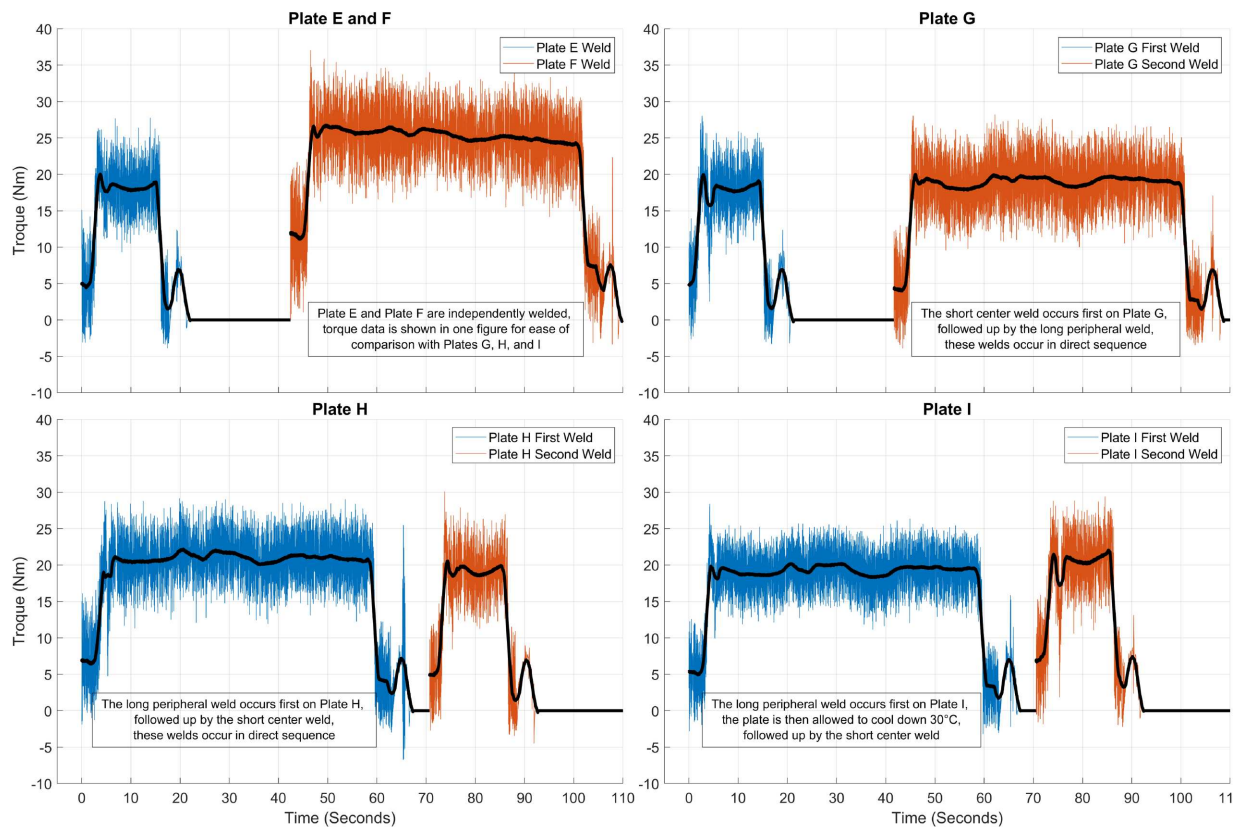


Figure 113: Spindle torque measurements during friction stir welding for each plate welded

Figure 114, featuring four thermocouples, demonstrates how the temperature generated by FSW influences the starting temperature of the plate for subsequent welds. For instance, thermocouples 1 and 2 (TC1 and TC2), both starting at nearly 25 degrees Celsius and positioned 21.5 millimeters from the edge of the 13-millimeter-wide weld seam, show similar temperature readings during the initial center weld at the bottom end of Plate G. After completing the center weld at around the 50-second mark, the peripheral weld begins, starting and ending near the same position by the exit hole at the bottom left of Plate G. TC1, positioned on the left side, first registers a rise to 80 degrees Celsius. Shortly after that, as the peripheral weld encircles the plate and the welding tool approaches the right side near TC2, the temperature at TC2 reads 10 degrees higher than the initial condition, illustrating the additional heat introduced by the welding sequence. This increase indicates that the starting temperatures for welds performed after an initial weld will be elevated, which, interestingly, does not significantly impact torque readings.

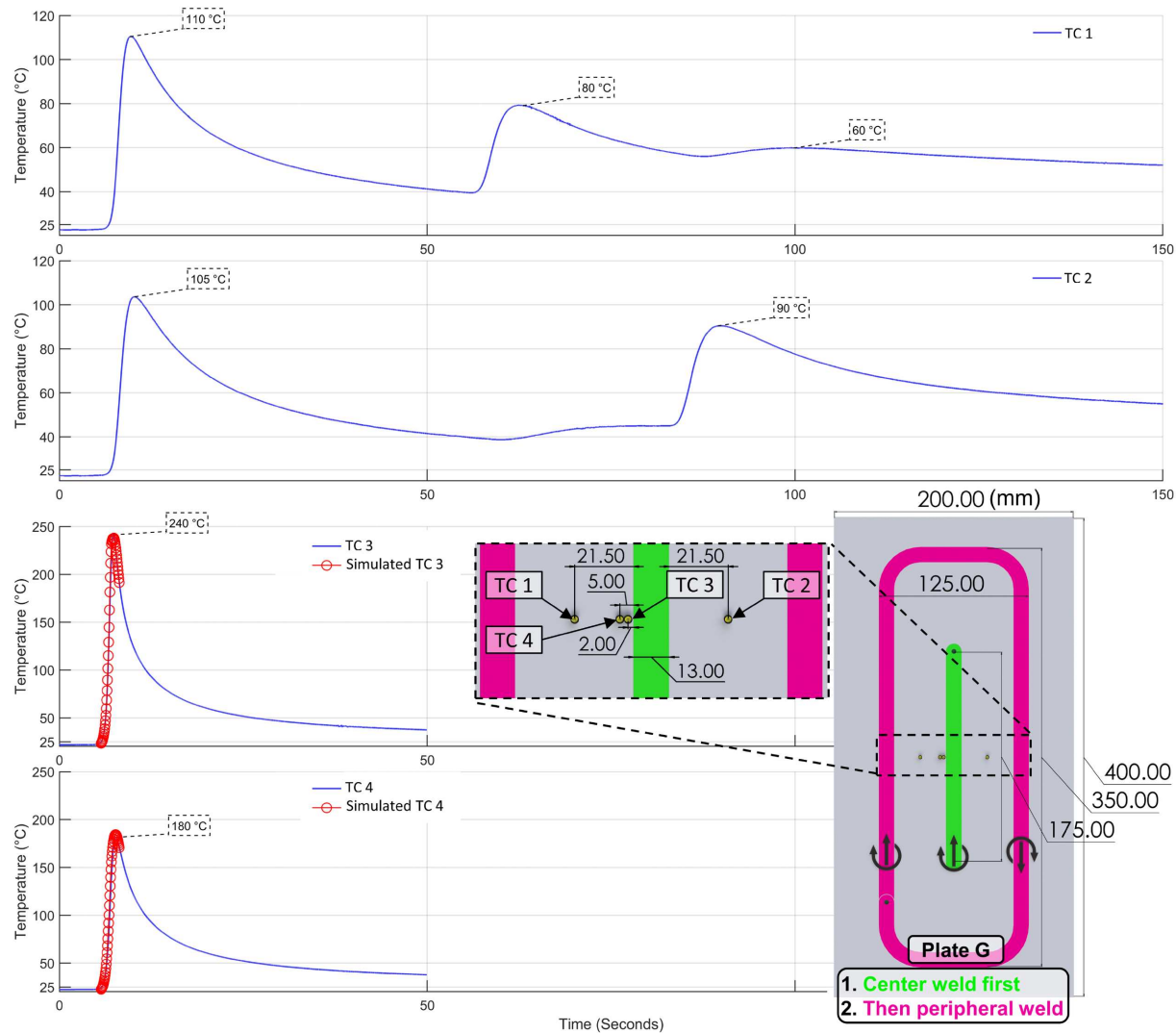


Figure 114: Measured temperature on four different thermocouples for Plate G during welding

In FSW, the torque required to move the tool through the material primarily depends on material properties, tool geometry, and process parameters like rotational speed and traverse speed. The findings between Figure 113 and Figure 114 suggest that variations in starting temperature do not significantly influence the torque because the process inherently accommodates a wide range of temperatures due to friction-generated heat. This heat leads to the thermal softening of the material, which is more significant than the initial temperature in determining the torque required. Therefore, it could be hypothesized that while initial plate temperature might cause minor fluctuations in starting torque, these variations are likely overshadowed by the more substantial temperature changes and material softening caused by the welding process. This hypothesis might explain why initial temperature variations don't significantly affect torque, as any minor changes

are quickly mitigated by the intense heat generated during welding, which dominates the material's response to the welding tool. This view aligns with a review article by Meyghani et al. [231] and studies completed by Sigl et al. [232] discussing methods for controlling welding temperature to ensure quality. The stability of torque despite variations in initial temperature could be due to the rapid thermal dynamics within the FSW process, where the temperature quickly reaches levels that diminish the impact of any initial temperature differences.

Moving on to TC3 and TC4, positioned just outside the weld path at two millimeters and five millimeters from the weld edge, respectively (see Figure 114), these thermocouples register much higher temperatures. TC3 reaches a maximum of 240 degrees Celsius, while TC4 peaks at 180 degrees Celsius, highlighting a significant temperature decay rate between them. The data from these thermocouples are explicitly used to validate the SPH simulation data. As depicted in Figure 114c and Figure 114d, TC3 and TC4 are strategically placed to capture essential temperature gradients across the weld area, just outside the direct influence of the welding tool. The setup for TC3 and TC4 allows for a comparison of real-world temperature readings with the simulation, ensuring that the SPH model accurately replicates the thermal dynamics of FSW and reinforcing the model's validity and reliability. Figure 115 depicts a specific material segment being tracked over time, creating a temperature contour in Figure 116. As the simulation progresses, the FSW tool approaches and physically interacts with the analyzed material segment, displacing material points and resulting in a blank spot (see Figure 115 and Figure 116) on the temperature data map at the point of material displacement, which is subsequently refilled.

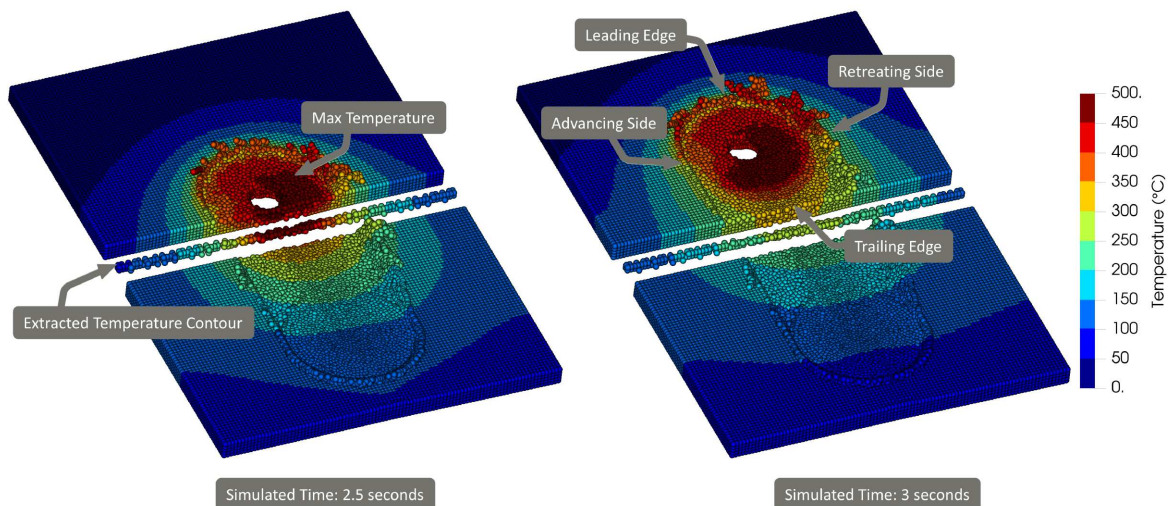


Figure 115: SPH simulation of center friction stir weld

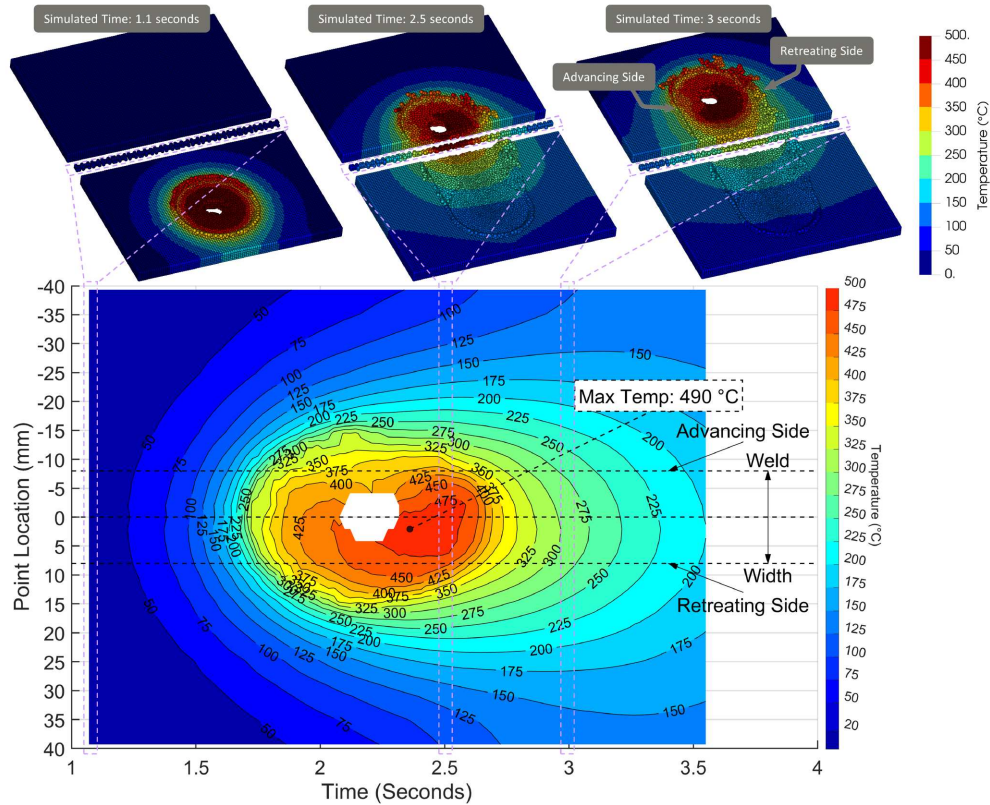


Figure 116: Temperature contour illustrating the thermal gradients of the stationary material over the simulation period for friction stir welded

The contour image (Figure 116) does not represent the traverse or rotational direction of the tool in any physical sense. Instead, it records and displays the temperature at a specific, fixed location of material over time (see Figure 115). At time zero, the material is at a constant room temperature. The simulation does not include a plunging phase; it begins at 1.1 seconds. The first recorded 'simulation time' is 0.02 seconds, which, when multiplied by the velocity scaling factor of 50 (as defined in the simulation), results in 1.07 seconds. This marks the actual start of the simulation, not zero.

The velocity scaling factor is used to expedite the simulation, calculating only a fraction of the actual time in 'simulation time' and then multiplying the result by the velocity scaling factor to obtain 'real-time.' Fraser [190] explains that this is achieved using velocity scaling (or time scaling), which does not alter the time step size. Instead, it allows more simulation to be performed in a reduced timeframe by scaling all relevant terms by a velocity scaling factor. These terms include rotational speed, traverse speed, thermal conductivity, convection coefficient, and other

related parameters. It is essential to exercise caution when applying this method, as Fraser [190] emphasizes, to ensure that the system's dynamics are not unreasonably altered.

Notably, in Figure 116, the maximum temperature consistently occurs at the trailing edge of the FSW process, which lies on the retreating side of the tool. This finding aligns with Silva-Magalhães et al. [34] measuring maximum temperature on the retreating-trailing edge of the FSW tool. This area reaches a maximum temperature of 490 °C, which is significant yet still below the melting point of aluminum, with the solidus temperature for 6061 and A365 at 590 °C and 570 °C respectively (Jin et al. [233]; Hernandez et al. [234]). The maximum temperature of 490 °C from the simulation is consistent with other physical measurements recorded by researchers in previous studies (Tang et al. [235]). Lastly, thermocouples placed 21.5 millimeters from the weld's edge, as shown in Figure 114, register temperatures around 100 degrees Celsius. The thermocouple measurements correlate well with the simulated maximum temperatures occurring approximately 30 millimeters from the center on both the positive and negative sides of the weld, reaching between 100 and 125 degrees Celsius. Heat distribution throughout the plate is evident as the simulation advances, potentially impacting subsequent welds nearby. Instead of starting at the typical 20-25 degrees Celsius for new welds, the starting temperature in the area increases to about 80 to 100 degrees Celsius due to lasting heat from previous welds. This approximately 60-degree increase from the initial conditions could significantly influence the thermal dynamics and quality of subsequent welding processes in the vicinity.

The temperature differences resulting from the initial weld sequence significantly impact the distortion observed in Plates G, H, and I (see Figure 112). Plates G and H, although exhibiting some variances in distortion—with Plate H showing more pronounced negative distortion at minus one millimeter near the peripheral weld and Plate G displaying localized negative distortion at the center—both follow a similar welding sequence. This sequence involves one weld heating the plate, followed immediately by a second weld, whether peripheral or center. In contrast, Plate I demonstrate greater negative distortion near the top left of the plate and significant positive distortion where the plate tips curl upwards. This pronounced curling occurs because, unlike Plates G and H, Plate I undergoes a cooling period to ambient temperature between the first peripheral weld and the subsequent center weld. The higher starting temperatures for the second weld in Plates G and H, which do not cool down between welds, seem to mitigate distortion compared to

Plate I, where the cooling period leads to increased distortion. This observation suggests that maintaining a higher temperature between sequential welds can reduce distortion. In industrial applications, especially with larger components, it might be beneficial to perform all welds on one section of a component consecutively before allowing any cooling. The increase in the initial starting temperature of 60 °C appears to reduce the resulting distortion. Additionally, reducing the time between welds could offer significant advantages, ensuring rapid transitions from one weld to the next without lengthy pauses. This approach could be particularly important when tool changes are necessary. These initial findings open various avenues for further exploration and adjustment in welding practices to optimize outcomes and reduce distortion in larger industrial components. As demonstrated in the study by Sinclair et al. [236], preheating the workpiece before FSW can significantly reduce axial forces, provided the system allows these forces to vary. Notable reductions are observed in the 80 to 100 °C range, with maximum reductions of up to 43% at a preheated temperature of 200 °C. This reduction in axial force enhances material flow and improves welding quality. By incorporating preheating techniques, it is possible to achieve more consistent and reliable welding results, minimizing distortion and enhancing the overall structural integrity of welded components.

Furthermore, the initial starting temperature of subsequent welds may significantly affect residual stresses, which in turn contribute to distortion. Residual stresses, being the stresses that remain in the material after welding, are a primary cause of distortion. The temperature gradient in FSW is a critical factor in determining how the material around the weld zone experiences thermal expansion and contraction. When the initial temperature of the plate is elevated due to a previous weld, the second weld starts at a higher baseline temperature. This results in an altered temperature gradient as the tool passes through because the surrounding material's temperature is now different from what it would be if starting from ambient temperature. Residual stresses in FSW are primarily formed during the cooling phase when the material tries to contract but is restrained by the surrounding cooler, unyielded material. The contraction behavior changes if the initial temperature is higher, particularly in areas away from the immediate weld zone. This modified contraction can result in more complex residual stress patterns due to the differences in thermal contraction across the weld area. Essentially, the non-uniform cooling rates across the weld influence the magnitude and distribution of these stresses. These changes in stress formation could lead to welds with different mechanical properties or increased susceptibility to defects such as cracking or porosity,

especially if the residual stresses exceed the material's strength limits. Understanding these dynamics could be important in predicting and mitigating potential weaknesses in the weld. Therefore, controlling the initial temperature and managing the welding sequence is critical in practice. Techniques such as pre-heating or controlled cooling might ensure more uniform temperature gradients and minimize the impact on residual stress. Raising the initial temperature by preheating the component before FSW could be advantageous. Additionally, applying post-weld thermal insulation, such as a thermal blanket or a cooling chamber, can reduce temperature gradients and allow for more even and gradual cooling. However, the specific benefits of these techniques on residual stress and distortion should be investigated further.

4.2.4 Residual Stress of Multi Welded Plates

Figure 117 details the locations where residual stress measurements were taken using neutron diffraction scans. These measurements involve comparing neutron diffraction data from both stress-free and stressed samples. The stressed samples are the plates themselves, while the stress-free samples are a set of matchsticks cut from each welded subsection. These samples are spaced 2mm apart over a distance of 200 millimeters, covering the dynamically recrystallized zone (DXZ), the thermomechanically affected zone (TMAZ), the heat-affected zone (HAZ), and the base material. This setup provides a stress-free reference for each material type encountered in FSW, as detailed in the section “4.1.4 Residual Stress on Single Welded Plates”.

The present section focuses primarily on the residual stress results rather than the specifics of the calculations and the use of stress-free samples, which are extensively covered in earlier sections. Figure 117 illustrates three lines on each plate for measurement: a blue line at 0 millimeters, a red line at 80 millimeters, and a green line at 160 millimeters. While the spacing between these points may vary slightly across different plates to optimize beam time, each point undergoes measurement in three directions: longitudinal (parallel to the weld), transverse (perpendicular to the weld), and normal (perpendicular to the surface). Additionally, Figure 117 highlights the negative and positive directions relative to the advancing and retreating sides of the weld, respectively. Small icons within Figure 117 indicate the direction of tool travel and rotation, helping to identify the advancing side for each weld.

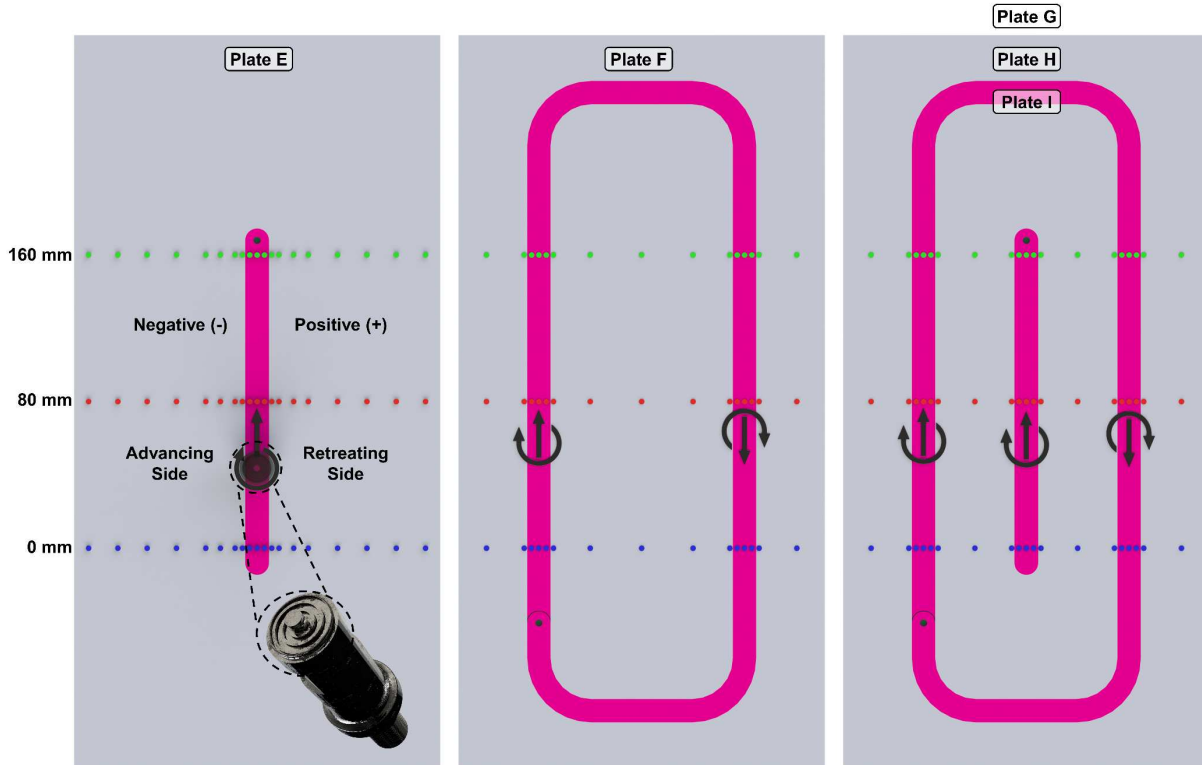


Figure 117: Blue line (0 mm line); Red line (80 mm line); Green line (160 mm line); layout for residual stress analysis on each plate

Figure 119 presents the residual stress data for plates E and F along the zero-millimeter line in the longitudinal direction. The top-left of Figure 119 consolidates the residual stress measurements of Plates E and F for easier comparison with Plates G, H, and I. Plate E shows significantly higher residual stresses on the advancing side compared to the retreating side at the start of the plate, with a 50-megapascal difference and the lowest stress point at 76 megapascals in the dynamically recrystallized zone (DXZ) at the center of the weld bead. This asymmetrical "M" shaped distribution is typical in friction stir welds, influenced by composition, texture, and temperature gradients that ultimately affect the yield strength in different weld locations (see Figure 118).

The yield strength results from Figure 118 are directly compared to the residual stress results in Figure 119. In the center of the weld, low yield strength results are recorded, while the region just outside the weld edge measures a yield strength of 246 MPa, with the weld edge itself measuring 179 MPa. The maximum tensile residual stress (see Figure 119) is measured just outside the weld between the two yield strength measurements on the advancing side of the tool in Figure 118. The key takeaway is that the yield strength limit varies across the weld. As noted in section “4.2.1.3 Profilometry-based Indentation Plastometry,” the measurements were conducted under non-ideal

conditions. Consequently, while it is reasonable to assume there are changes in yield strength across the weld, the precise measurements may not be entirely accurate. Therefore, the results from Figure 118 are useful for a qualitative assessment of anisotropy across the weld.

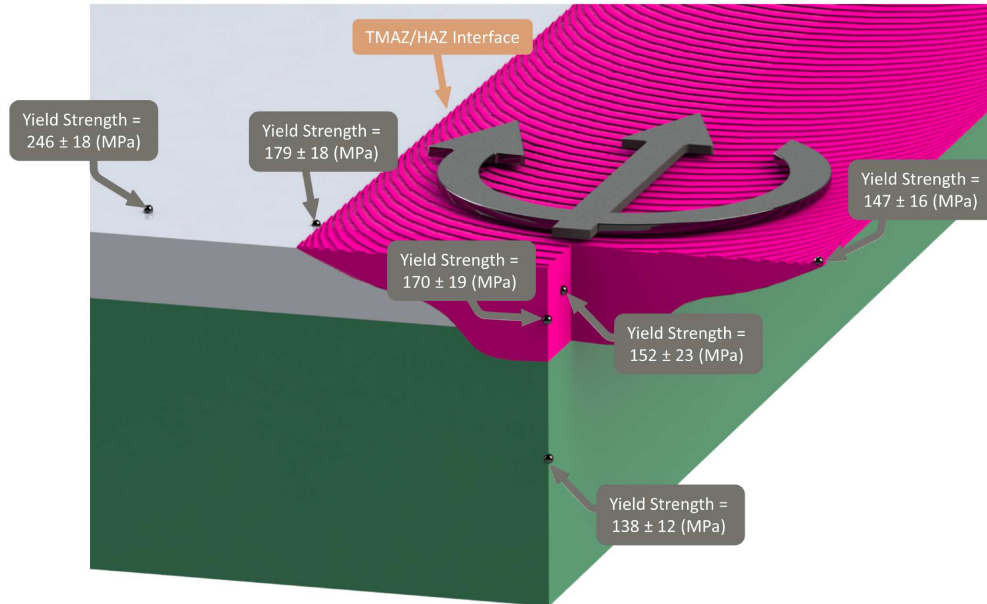


Figure 118: Profilometry-based Indentation Plastometry (PIP) yield strength results from friction stir weld

The residual stress measurements coincide with the yield strength in certain areas but not others, where the yield strength acts as the upper limit for residual stress generation. However, it's important to reiterate that, strictly speaking, it is not a definitive limit. Stresses may evolve above the yield strength, as seen in some areas of the battery tray, or even above the ultimate tensile strength in "locked" material. In many cases, though, such as this one, the stresses evolve up to the yield strength level when there is significant unrestricted plasticity (deformation) involved. The area where the yield strength does not coincide with residual stress is along the trailing edge of the tool in the center of the weld, which experiences the highest temperatures for the longest duration (see Figure 116). Consequently, the temperature gradient is less intense down the center of the weld compared to the edges, resulting in the valley on the measured "M" shape profile in the longitudinal direction as the residual stresses are generated due to the contraction of the material during cooling.

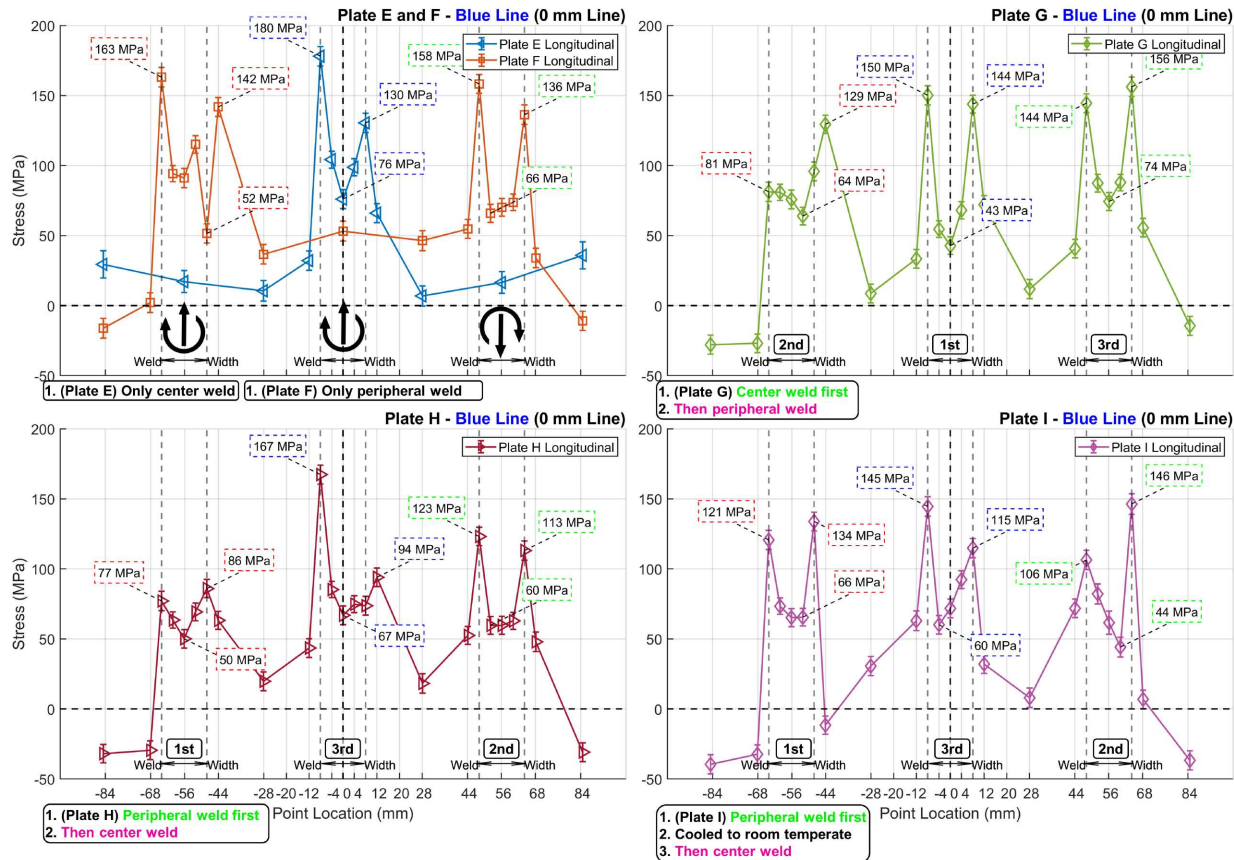


Figure 119: Blue line (0 mm line) (see Figure 117); Stress Direction: Longitudinal

However, at the weld edge on the advancing side near the TMAZ and HAZ interface, the maximum value of residual stress (180 MPa; see Figure 119 Plate E) almost directly coincides with the values measured in yield strength (179 MPa; see Figure 118). Any thermo-mechanical loading above this level is released into plastic deformation (distortion). The yield strength of A6061 is typically 240 MPa in the T6 condition, but near the weld, the strengthening is removed when the material experiences high welding temperatures.

In Plate F, which features only a peripheral weld encircling the plate, there is a distinct, albeit less pronounced, asymmetrical M-shape. The stresses on both advancing and retreating sides are closer together, illustrating less disparity in the "M" shape, indicative of interacting residual stress fields and varying temperature gradients due to the weld's circumnavigation around the periphery of the plate. The peripheral weld's interaction at the 56-millimeter mark shows closer residual stress levels between the advancing and retreating sides, differing from the typical pattern where the advancing side exhibits maximum stress in the single center welds like Plate E. The distortion measurements in Figure 112 indicate substantial differences between Plates E and F. Plate F

exhibits more pronounced negative distortion at the center, with the top left and bottom right corners showing positive curling. This pattern correlates with lower residual stresses observed in these areas, suggesting that interacting residual stress fields contributed to distortion. According to Radaj [237], the temperature and microstructural state fields in welding are coupled to the stress and deformation fields affected by thermal and transformation stress, respectively. As the material distorts, it may partially relieve these stresses, only to develop new residual stress patterns influenced by the temperature distribution and the material's deformation history. Plate F, which lacks a center weld but includes a peripheral weld (see Figure 112), has significantly different distortion and longitudinal residual stress patterns in Figure 119. Maximum tensile residual stresses are lower in Plate F compared to Plate E, possibly because the edges of Plate F, with less material support due to the location of the welds, cannot sustain the imposed stress and thus release it through deformation. In Plate F, the central areas between the welds at the 0 mm location show lower residual stresses, but the difference is not as pronounced as in Plate E, where residual stresses outside the weld approach zero on both sides of the center weld. However, near the edges of Plate F, the stress state shifts into compression, particularly at points -84 and 84 millimeters. While the tensile residual stresses are reduced for Plate F, the breath of stress extends into the compressive region, and the center exhibits elevated tensile stresses, possibly due to the interaction of residual stress from adjacent welds. This results in greater compressive stress responses near the plate's edges, which may contribute significantly to the larger distortions measured in Plate F (see Figure 112). This greater compressive stress response is not unique to Plate F. On Plates G, H, and I—each with both welds—a shift to compression near the plate's edge is observed.

Plate G's longitudinal stress profile, as depicted in Figure 119, features a center and peripheral weld, with the welding sequence starting on the center weld followed by the peripheral weld. These are detailed in the annotations as the first, second, and third welds, respectively. Although the second and third welds are part of a single circular peripheral weld, they are segmented in the figure to clarify the sequence of operations. The center weld in Plate G shows lower and nearly equal tensile stresses between the advancing and retreating sides, unlike the standalone center weld on Plate E. This difference is likely due to the temperature interactions and the influence of residual stress fields from the subsequent peripheral weld. The welds on Plate G located at the negative 56-millimeter mark (second in sequence) and the positive 56-millimeter position (third in sequence) are affected by the heat from the initial center weld. This heat generation elevates the plate's

temperature, altering the temperature gradients during cooling and impacting the residual stress levels observed.

In Plate H, the welding sequence begins with the peripheral weld, reversing the order used in Plate G. When comparing Plates G and H in Figure 119, where the sequence of welds is the only variable, it is observed that Plate H exhibits lower tensile residual stresses throughout the peripheral weld. Additionally, there is a notable difference in tensile stresses between the advancing and retreating sides in the center weld. This difference demonstrates that the sequence in which the welds are performed influences the effects on the initial weld. Specifically, in Plate H, there are lower tensile stresses on the peripheral weld closer to the end of the plate but higher maximum tensile stress in the middle. This alteration in stress distribution could impact the component's life. If changing the welding order can reduce high tensile stresses on the weld near the plate's edge—where there is less material to provide support and a higher susceptibility to crack propagation—then this represents a potential advantage to completing peripheral welds first in sequence. Nonetheless, the distortion profiles of Plates G and H are similar, suggesting that the sequence may not significantly impact the overall outcome of deformation (see Figure 112).

Plate I adopts a different strategy: the peripheral weld is completed first and allowed to cool to ambient temperature before the center weld is executed. In this configuration, the peripheral weld, particularly at the negative 56-millimeter location, does not achieve the low tensile residual stress seen in Plate H despite the peripheral weld being performed first. Moreover, Plate I's center weld exhibits stress levels comparable to those in Plate G and lower than those in Plate H but shows significantly higher distortion. A comparison of the CMM measurements from Plates H and I, as shown in Figure 112, highlights these differences in distortion. Plate I displays substantial positive distortion at the top right and bottom left corners, in contrast to Plate H.

Progressing to the 80 mm position on the plate (see Figure 117, red line), a similar pattern of longitudinal residual stress is observed, as shown in Figure 120, comparable to the 0 mm position in Figure 119, where primarily the second weld influences the residual stresses of the first weld. While the overall patterns are consistent, specific points display differences in magnitude. In the steady-state region of the weld, observing areas with lower longitudinal residual stresses is typical. This occurrence is likely linked to the consistent application of heat as the welding process transitions from the initial cold start to the more thermally stable steady-state phase. Unlike the

beginning, which starts from a colder state, or the end, which might experience an abrupt cessation of heat, the steady-state region benefits from a continuous and uniform heat application. This consistent thermal gradient could be the reason for the reduction in residual stresses observed at specific points within this region.

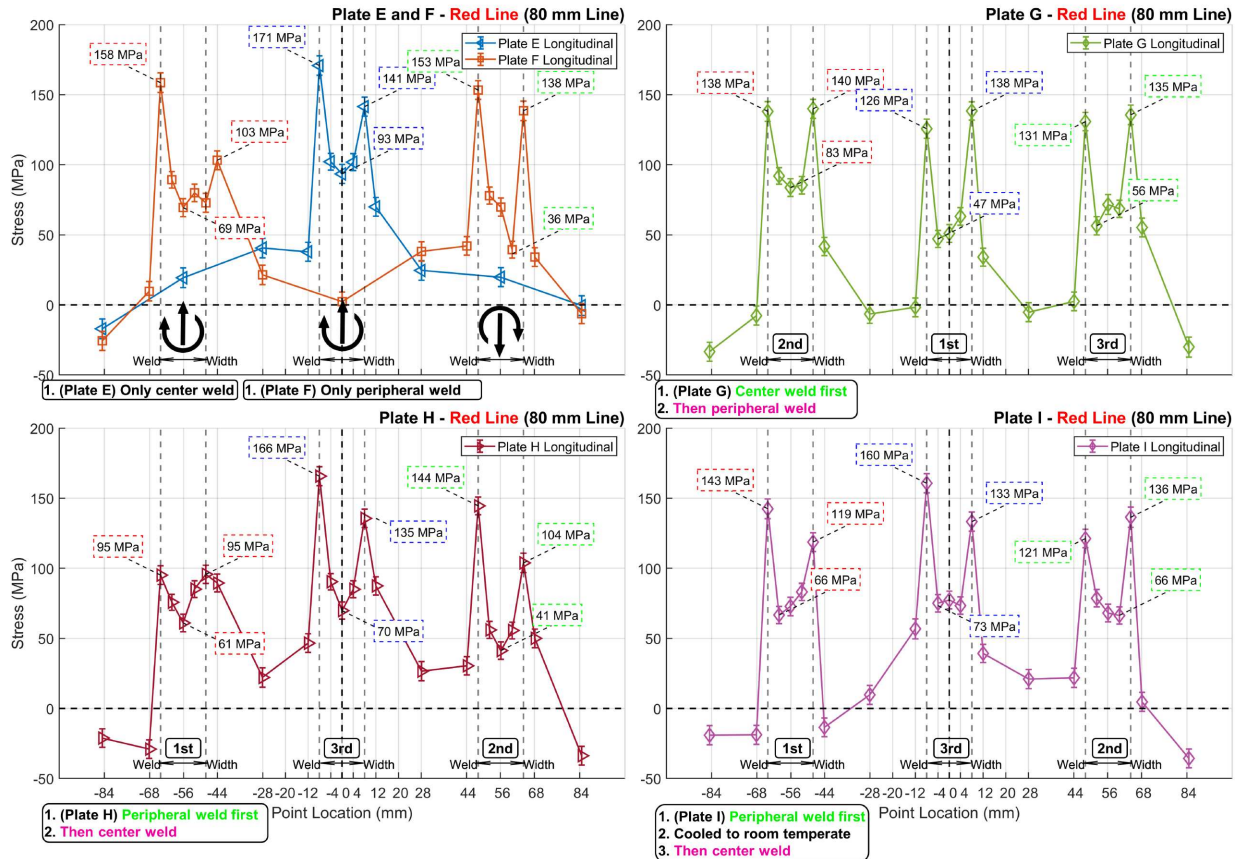


Figure 120: Red line (80 mm line) (see Figure 117); Stress Direction: Longitudinal

However, the changes in residual stress levels are not markedly pronounced across the board in Figure 120 compared to the 0 mm blue line in Figure 119. Only a few points show notable reductions. This consistent lack of magnitude reduction contrasts with single-weld studies, where an apparent residual stress decrease is often evident in the steady-state region. The subtler changes in multi-weld scenarios are likely due to the complex interactions of multiple welds within this area, affecting how stresses are distributed and relieved during the process. These more unique residual stress distributions are probably a function of both temporal and spatial dynamics. When considering the temporal dynamics, the residual stress field of an initial weld is established as it cools and the metal contracts. If a second weld is introduced nearby before the entire structure equilibrates, the heat from the second weld can modify the still-evolving stress field of the first

weld. The effect of this modification most likely comes down to its spatial position, where the closer the welds are, the more likely their heat-affected zones will affect each other's stress fields.

Lastly, considering the 160mm scan line marked by the green line for longitudinal stresses (see Figure 121), this area presents some of the most complex interactions observed in the study. Plate E, a single-welded plate, exhibits a significant increase in residual stress, ranging between 20 to 30 MPa higher than earlier measurements at the 80mm and 0mm locations.

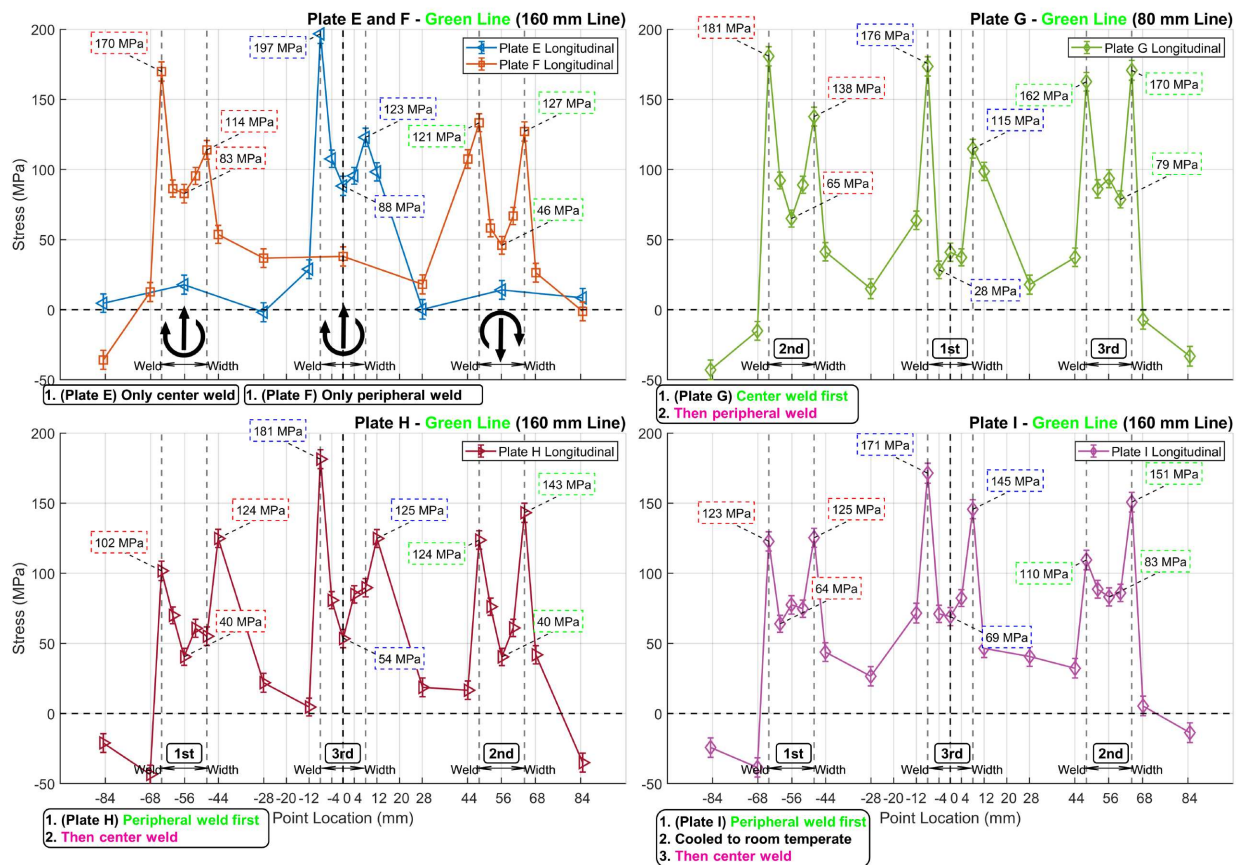


Figure 121: Green line (160 mm line); Stress Direction: Longitudinal

This pattern of high residual stresses at the 160mm line is consistent across plates G, H, and I, where this line marks the end of the center weld. However, for the peripheral weld, this line coincides with the steady-state region. The welding sequence significantly impacts the residual stresses observed for plates G and H on the 160mm scan line. Plate G, with the center weld conducted first, shows higher residual stresses at this green scan line before the peripheral weld, resulting in elevated stresses around the periphery. In contrast, Plate H starts with the peripheral weld, which shows lower residual stresses at this line.

However, when the center weld is subsequently performed, it matches or exceeds the stresses seen in Plate G. This suggests that temperature distributions play a crucial role, as evidenced by comparing these plates with Plate F, which only features a peripheral weld starting at the negative 56mm location. In Plate F, the peripheral weld begins on the colder side of the plate; as it wraps around to the positive 56mm location, the stresses significantly drop by about 50 megapascals.

This decrease may be attributed to either geometric effects or temperature variations influencing the weld's progression. Notably, the end of the peripheral weld in Plates G, H, and I do not show the high residual stresses near the 200 megapascal level observed at the center weld's end. This difference is likely due to the peripheral weld's geometry, which does not transition from weld into no-weld or base material as sharply as the center weld. The peripheral weld completes a circuit, ending on itself, which may contribute to a more uniform distribution of stresses and or further release of stress into the geometry as distortion due to the lack of material support on the peripheral welds as they are completed near the plate's edges. The green line, while in a steady state for the peripheral welds, is near the end of the plate for the center weld, capturing the culmination of welding stresses.

In contrast, the closest scan line to the end of the peripheral weld, marked by the blue line, is significantly further away. This is illustrated in Figure 117, at the bottom left of the multi-welded plate, showcasing the exit hole of the peripheral weld. Yet, it does not experience the pronounced ending stresses seen in the center weld, emphasizing the unique impact of the weld's geometrical closure on residual stress distribution. This analysis underscores the importance of considering both the welding sequence and the geometrical configuration of the welds in understanding and predicting residual stress distributions and their potential implications for the structural integrity of welded components.

Examining the transverse stresses is also important, as both types significantly affect the component's structure, integrity, and performance, as well as significantly influence the welded joint's ultimate strength, fatigue life, and fracture toughness. Longitudinal stresses, running along the weld seam, primarily result from thermal expansion and contraction as the tool moves forward and are a common cause of bending. Conversely, transverse stresses occur perpendicular to the weld direction, often due to differential cooling across the weld's width. These stresses are influenced by temperature gradients extending from the advancing and retreating sides of the weld

to the HAZ and the base material regions unaffected by the tool's mechanical disturbance. The maximum temperature is typically recorded on the trailing edge of the tool, with maximum temperature gradients originating from this location outward (see Figure 115). Different temperature gradients between these sides are widely acknowledged, as is in the case of the present study (see Figure 115). However, the intensity of the gradient depends on material type, tool design, and welding parameters, to name a few factors. Transverse stresses are critical to consider because, unlike longitudinal stresses, which may lead to cracks along the weld seam, high transverse stresses can cause transverse cracking similar to what was observed by Milan et al. [238]. The previous section, “4.1.4 Residual Stress on Single Welded Plates”, concluded that transverse stresses are inconsistent throughout the weld, tending to increase towards the end. This increase is highlighted in the comparison between Figure 122 and Figure 123, where Figure 122 represents the start and Figure 123 the end of the weld at the 160-millimeter line, showing an increase in transverse stresses of 20 to 30 megapascals, particularly noticeable in the center weld. This pattern is unique to the center weld, demonstrating the plates' varying ability to support stresses near the edges, especially in the transverse direction, extending to the plate's edge more quickly than in the longitudinal direction. Compared to Chapter 3, with similar weld paths but different supporting geometry as the component studied is a battery tray, more uniform transverse residual stresses were measured across all welds. This uniformity is attributed to the tray's design, featuring a lip that forms a container shape capable of better supporting these stresses and preventing them from converting into distortion.

This geometry suggests that designs incorporating more support can retain higher residual stresses, potentially reducing distortion. The direction and magnitude of these stresses are important in determining how the material will perform under service loads, a key consideration for structural applications where safety and reliability are crucial. Understanding the stress profiles in both directions is vital for designing an optimal welding configuration for the structure. It also informs decisions regarding potential post-weld treatments such as heat treatment, mechanical stress relief processes, or adjustments in the assembly's layout or sequencing of welding paths.

The last direction of consideration, normal stress direction (see Figure 124), typically remains minimal due to modest temperature fluctuations in this orientation and the absence of significant constraints (Williams et al. [82]).

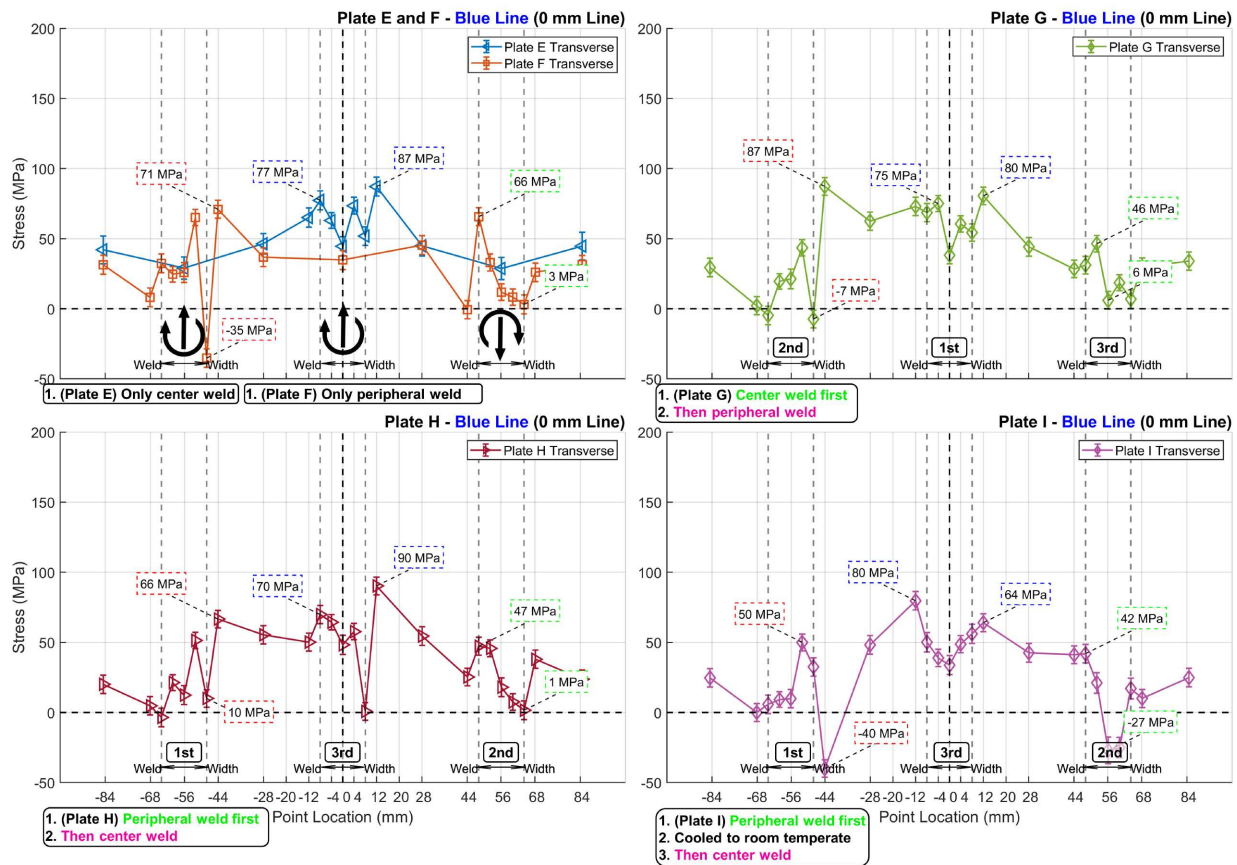


Figure 122: Blue line (0 mm line); Stress Direction: Transverse

Figure 124 encompasses the normal direction of stresses at the 160 mm mark. This direction of stress is by far the most consistent across the start, middle, and end of the weld, with tensile stresses generally not exceeding 55 megapascals; therefore, only the ending 160mm line residual stresses are provided. The most notable feature of the normal profile is the many points near each weld edge exhibit a compressive reaction, underscoring the complex interplay of stresses within welded components. While the normal stress direction shows relatively minimal fluctuations and consistent behavior, this stability is significant as it indicates a relatively uniform thermal profile across the depth of the weld.

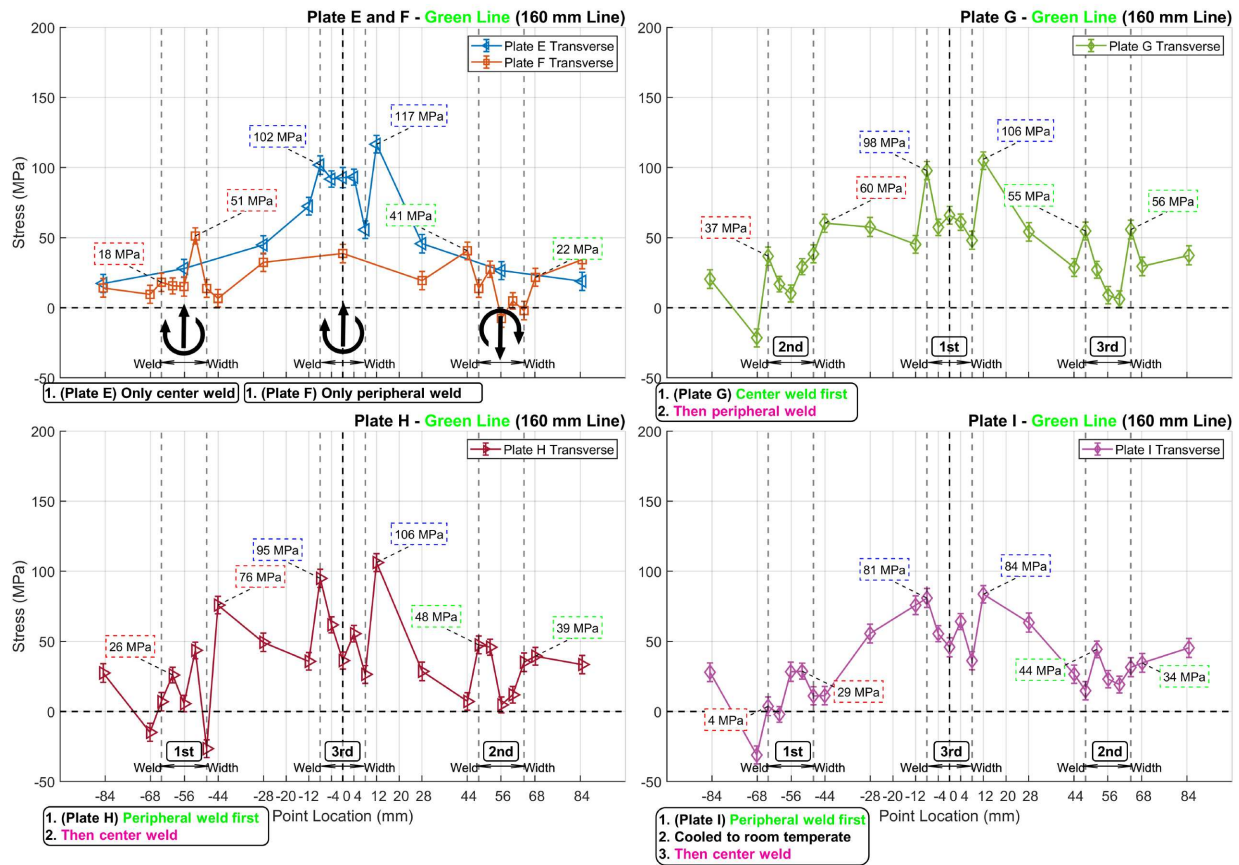


Figure 123: Green line (160 mm line); Stress Direction: Transverse

It is important to consider the plane-stress conditions that exist in the 6061 2mm plate, especially when evaluating stress distributions away from the weld zones. Plane-stress conditions imply that the stress normal to the plate surface is negligible, while in-plane stresses (longitudinal and transverse) dominate. This assumption is crucial for thin plates like the 2mm 6061 used in this study, where the thickness is small relative to the other dimensions. However, the observed normal stress of approximately 25 MPa in regions distanced from the weld, as shown in Figure 124, indicates that plane stress conditions might not be perfectly met. This non-zero normal stress suggests that residual stresses from the welding process are still present and significant, even in regions far from the weld. These residual stresses can contribute to overall distortion and affect the mechanical behavior of the welded component.

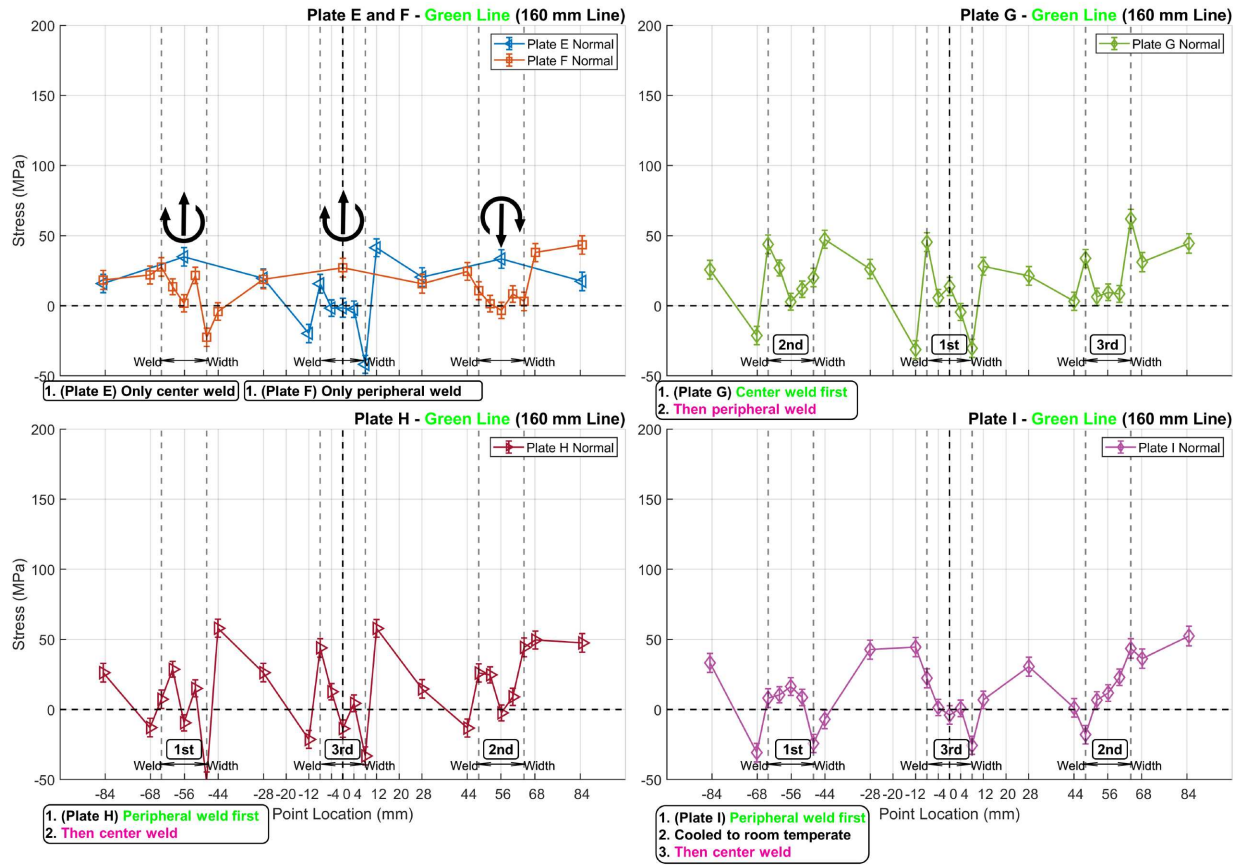


Figure 124: Green line (160 mm line); Stress Direction: Normal

Additionally, many neutron diffraction studies may omit the fabrication of stress-free samples and rely on the plane stress assumption to calculate the d_0 spacing. This study confirms that such an approach may significantly affect the results of neutron diffraction stress scans. Therefore, by ensuring that the plane-stress assumption is valid or by accurately measuring deviations from this condition, one can better predict and mitigate potential distortions and stress concentrations. This leads to improved weld quality and structural integrity, particularly in thin plates where out-of-plane stresses should ideally be minimized to prevent buckling and other forms of deformation.

4.2.5 Section Conclusions

This comprehensive data set and analysis provided in the current study serve as a baseline for simulations and technologies aimed at exploring distortions and interactions among multiple welds on the same friction stir welded component. It can be leveraged for specific applications, such as in battery trays and other structures where FSW is employed, particularly where flatness is critical.

The findings suggest that while weld sequencing may not significantly affect outcome distortions, managing thermal gradients and minimizing wait times between welds are crucial for reducing distortions and achieving more predictable results. This insight could inform strategies to mitigate distortion through tailored weld management rather than sequence alteration alone. Within the scope of the data analyzed, for the aluminum 6061 lap weld to A365 cast structure, the following conclusions are made:

- (1) Torque measurements indicate consistency and independence from the welding sequence. Even when welds are conducted in succession, which raises the plate's temperature, the initial torque values are only minimally affected. This effect may be negligible in practical terms. In the steady-state phase of the weld, torque levels stabilize, demonstrating uniformity across different welding sequences.
- (2) Single welds introduce minimal distortion to the plate. However, as additional welds are implemented, the cumulative effect results in significantly increased distortion. This amplification underscores the impact of multiple welds on the structural behavior of the plate.
- (3) Residual stress resulting from multiple welds caused significant distortion in all plates. While the distortion profile did not change between certain ordered weld sequences, the magnitude of distortion remained consistent. The distortion magnitude only changed with added welds or when the plate temperature decreased due to introduced waiting periods between welds. However, altering the weld sequence changed the residual stress distribution on plates, where the only variable was the weld sequence. Initiating welding at the center, followed by the periphery, results in higher residual stresses at the edges. Conversely, starting at the periphery reduces high tensile stresses near the edges, potentially enhancing component longevity. Yet, the benefits of altering weld order diminish when the initial weld is allowed to cool, indicating that the residual stresses from the first weld significantly impact subsequent welding outcomes, which also increases the distortion magnitude and changes its profile. Thus, in larger components, maintaining a quick succession of localized welds may be advantageous, preserving the heat from initial welds to minimize stress and distortion.

- (4) Longitudinal residual stresses, measured as the most significant in the component, consistently exhibit higher values in scenarios involving multiple welds. Specifically, central welds with particular end geometries show the highest longitudinal residual stresses. In contrast, peripheral welds completing a circuit to end the weld tend to exhibit lower residual stresses. Furthermore, transverse stresses, which increase from the beginning to the end of the weld, are particularly pronounced in central welds. Peripheral welds, lacking direct material support, often show significantly lower or even compressive residual stresses, highlighting the influence of support and weld geometry on stress distributions in multiple directions.
- (5) Given the normal stresses in the single-welded plates, it was observed that the in-plane stresses were low when welding speeds were between approximately 10-40 MPa. As welding speeds increased, the average in-plane stress across the plate also rose. In the multi-welded plates, adjacent welds contributed to complex stress interactions, generating normal non-zero tensile stresses of around 50 MPa, even in the base material. When comparing the multi-welded plates to the welded high-pressure die-cast components from the battery tray, normal (Z) residual stresses were significantly elevated to approximately 100 MPa. This increase is due to multiple factors, including the influence of adjacent welds and the possible inheritance of residual stresses from the high-pressure die-cast component before welding.

Chapter 5: Conclusion

This dissertation was undertaken to address the critical need for efficient thermal management in electric vehicles, particularly focusing on the design of coolant channels within battery trays. These channels are crucial for maintaining optimal operating temperatures of battery cells, thereby maximizing the distance per battery kilogram achievable. The primary method explored for creating and sealing these channels involved high-pressure die casting (HPDC) followed by the sealing of coolant channels with thin aluminum plates using Friction Stir Welding (FSW). FSW, an advanced solid-state joining process, was chosen due to its ability to overcome significant challenges associated with traditional welding methods for aluminum, such as high oxidation, thermal distortion, and environmental impact. The research comprehensively investigated the formation and implications of residual stresses introduced by FSW, especially in multi-weld configurations. These stresses, if unmanaged, could potentially compromise the structural integrity of the battery tray, affecting both safety and functionality. Through detailed analysis using techniques like neutron diffraction, this study measured and analyzed the internal stresses and their redistribution post-straightening operations. The ultimate aim was to refine FSW parameters to enhance joint quality, minimize the need for costly post-weld treatments, and ensure robust, lightweight components conducive to sustainable and efficient electric vehicle operation. The conclusions drawn from this investigation provide foundational insights into optimizing FSW processes. The primary conclusions that can be drawn from this dissertation are as follows:

3.1 Characterization of FSW Microstructure and Residual Stress in the As-Welded Non-Straightened Battery Tray

- (1) The tool's rotation and transverse direction, with respect to the geometric constraints of the component, influence the residual stress profiles. For example, if the rotational and traverse direction switched, the stress profile would mirror along the vertical axis, with the maximum tensile stress consistently occurring on the advancing side of the tool rotation direction.
- (2) Unintentional preheating of the battery tray by welds occurring in the proximity of unwelded locations results in a decrease of maximum tensile stress after these unwelded locations become welded.

- (3) Microstructural marks and residual stress results indicate that the advancing side of the tool rotation is consistent with the maximum residual stress location, where the advancing side's maximum stress can be 70% higher than the tool's retreating side.
- (4) The EDS analysis of a weldment portion reveals the maximum silicon content through the 6061 to A365 FSW interface, suggesting that the plasticized pieces during the FSW operation primarily stir the material with minimal mixing, leading to a very narrow transition zone, on the order of 100 microns.

3.2 Microstructural Changes in Friction Stir Welds of the Battery Tray

- (1) ICP results indicate silicon accumulation during the FSW operation for both weld groups. However, as the coefficient of friction reduces with increasing temperature (proportional to the increasing rotational speed), silicon accumulation decreases in the weld. This reduction is observed in the 56 mm weld (high-temperature weld) compared to the 0 mm and 112 mm welds (lower-temperature welds), possibly because less material adheres to the tool at higher temperatures. In contrast, at lower rotational speeds, more material sticks to the tool, resulting in an ordered texture and higher Si accumulation in the weld. It is important to distinguish this from the stirring of the underlying A365 cast structure. In the lap weld configuration, the primary mixing occurs within the 6061 plate, with the A365 plate below experiencing minimal plastic deformation. The narrow transition zone discussed in the last section, indicated by the sharp cut-off in silicon content detected by EDS, suggests stirring rather than mixing with the A365 plate.
- (2) The distribution and generation of precipitates are not the main factors causing the increased surface hardness in the 56 mm weld compared to the 0-mm and 112-mm welds. Instead, the reduction in ordered texture or increased randomness due to higher plastic deformation, resulting from increased rotational speed, traverse speed, and plunging force, is found to cause the variation in mechanical properties between the weld groups.
- (3) KAM results show an increase in misorientation of the grains and suggest an increase in the density of dislocations due to higher rotational speeds, traverse speeds, and plunging forces. Additional increased misorientations indicate more severe plastic deformation. Although there is less sticking with increased rotational speed, the material that does stick experiences much higher plastic strain. The competing factors of increasingly less ordered

texture and higher plastic strain with increased rotational speeds may explain why welds conducted at higher rotational speeds have increased hardness.

- (4) With a constant speed ratio (rotational speed/traverse speed), competing mechanisms keep the grain size relatively consistent, only deviating by a few microns from each weld group as the rotational speed and traverse speed increase.

3.3 Analyzing Distortion and Stress Effects in Response to Straightening the Battery Tray

- (1) After the FSW process, the yield area (i.e., the area containing residual stress above the material yield strength) accounted for 24, 9.6, and 9.8% of the scanned area for the X-direction, Y-direction, and Z-direction of stresses, respectively.
- (2) The FSW process imparts significant plastic deformation, leading to large residual stresses shown in the yield area, causing distortions of the battery tray upwards of ± 3 mm at the wings and center of the battery tray, as compared to the pre-weld geometry.
- (3) Following the burnishing operation, the measured residual stress in the scanned region revealed evidence of surface layer compression induced by the burnishing technique. In addition, the burnishing operation reduced the yield area by 14 and 18% in the X-direction and Y-direction, respectively. In contrast, the Z-direction stresses increased the yield area by 10% after the burnishing operation. Nonetheless, the burnishing operation restored 93% of the battery tray's surface area to within ± 0.75 mm of the pre-weld geometry.
- (4) Following the coining operation, the yield area is reduced by 14% in the X-direction of stresses. The yield area in the Z-direction stayed nearly constant, only decreasing by 2%. However, the battery tray's significant distortion profile was reduced by the operation, which required considerable plastic deformation to restore the pre-weld geometry, increasing the yield area by 23% after straightening in the Y-direction of stress. Nevertheless, the coining operation restored 98% of the battery tray's surface area to within ± 0.75 mm of the pre-weld geometry.

4.1 Residual Stress Characteristics in Friction Stir Welding: An In-depth Temperature, Texture, and Neutron Diffraction Analysis of Speed Parameters

- (1) The torque required for welding decreases as the speed of the FSW tool increases while maintaining a constant speed ratio. This trend is observed as the speed incrementally rises

from Plate A, with settings at 700 mm/min and 1100 RPM, to Plate B at 950 mm/min and 1500 RPM, Plate C at 1150 mm/min and 1800 RPM, and finally to Plate D at 1400 mm/min and 2200 RPM.

- (2) With increasing welding speeds, the maximum temperature achieved during welding rises for plates B, C, and D, with temperatures of 490°C for plate B, 515°C for plate C, and 550°C for plate D. Additionally, thermal exposure diminishes with faster welding speeds, resulting in less total thermal exposure due to increased traversing speed.
- (3) Stress-free measurements vary with different welding speeds, but these variations do not follow a consistent pattern and are specific to the speeds applied. The main differences are associated with the stirring and rotating actions of the pin, which create distinct zones from the weld's center to its edges. This effect is thought to result from the segregation of particular elements within the alloy, leading to changes in stress-free reference values. Consequently, this creates a requirement for multiple stress-free measurements in diffraction-based studies to accurately determine residual stress levels.
- (4) Longitudinal stresses escalate with increased welding speeds, ranging from a minimum peak stress of 170 MPa for plate A to a maximum peak stress of 230 MPa for plate D. This trend is supported by SPH simulation temperature gradients, showing plate D with the most intense thermal gradients and the most significant residual stress reactions.
- (5) SPH simulations show that maximum plastic strain is predominantly observed on the advancing side of the tool pin, almost doubling the strain compared to the retreating side. The peak strain is located in the pin region rather than the tool's shoulder area.
- (6) Across the center of the weld on Plates A, B, C, and D, normal and transverse stresses are minimal in comparison to longitudinal stresses. However, as measured for Plate A, steep thermal gradients significantly influence transverse stresses, starting with an average in-weld stress of 73 MPa at the beginning, decreasing to 32 MPa in the center, and then rising to 103 MPa at the weld's end. The maximum stresses in the transverse direction, along with the peak stresses in the longitudinal direction of 190 MPa at the weld's end, might account for the frequent instances of failure observed near the weld's exit.
- (7) As welding speeds increase, there is a noticeable reduction in maximum texture intensity, decreasing from 4.83 for Plate A to 4.47 for Plate B, then to 4.09 for Plate C, and finally to 3.35 for Plate D. Despite these changes, the average grain size within each plate's weld

zone (DXZ) remains consistent, measuring within a few microns of 17 μm due to the constant speed ratio utilized. The faster welding speeds and reduced texture may diminish the anisotropic impact, potentially through the mechanism of texture-induced grain coarsening, on yield strength. This reduction in texture allows for higher and/or altered yield strength values in the weld material from Plate A to Plate D, which may affect the residual stress response that is closely linked to the material's yield strength. Therefore, texture may indirectly impact residual stress by influencing yield strength.

- (8) The distribution of Schmid factors within the friction stir weld seems to significantly contribute to the influence of mechanical properties (i.e., the yield strength). Regions with high Schmid factors, particularly by the near tool pin advancing side, may facilitate localized plastic deformation and reduce the ability to retain residual stress. Conversely, areas with interlaced high and low Schmid factor grains hinder slip, potentially contributing to the build-up of tensile residual stresses.

4.2 Influence of Multiple Welds on Residual Stress and Distortion in Friction Stir Welding: A Neutron Diffraction Study

- (1) Torque measurements indicate consistency and independence from the welding sequence. Even when welds are conducted in succession, which raises the plate's temperature, the initial torque values are only minimally affected. This effect may be negligible in practical terms. In the steady-state phase of the weld, torque levels stabilize, demonstrating uniformity across different welding sequences.
- (2) Single welds introduce minimal distortion to the plate. However, as additional welds are implemented, the cumulative effect results in significantly increased distortion. This amplification underscores the impact of multiple welds on the structural behavior of the plate.
- (3) Residual stress resulting from multiple welds caused significant distortion in all plates. While the distortion profile did not change between certain ordered weld sequences, the magnitude of distortion remained consistent. The distortion magnitude only changed with added welds or when the plate temperature decreased due to introduced waiting periods between welds. However, altering the weld sequence changed the residual stress distribution on the plates, where the only variable was the weld sequence. Initiating welding at the center, followed by the periphery, results in higher residual stresses at the

edges. Conversely, starting at the periphery reduces high tensile stresses near the edges, potentially enhancing component longevity. Yet, the benefits of altering weld order diminish when the initial weld is allowed to cool, indicating that the residual stresses from the first weld significantly impact subsequent welding outcomes, which also increases the distortion magnitude and changes its profile. Thus, in larger components, maintaining a quick succession of localized welds may be advantageous, preserving the heat from initial welds to minimize stress and distortion.

- (4) Longitudinal residual stresses, measured as the most significant in the component, consistently exhibit higher values in scenarios involving multiple welds. Specifically, central welds with particular end geometries show the highest longitudinal residual stresses. In contrast, peripheral welds completing a circuit to end the weld tend to exhibit lower residual stresses. Furthermore, transverse stresses, which increase from the beginning to the end of the weld, are particularly pronounced in central welds. Peripheral welds, lacking direct material support, often indicate significantly lower or even compressive residual stresses, highlighting the influence of support and weld geometry on stress distributions in multiple directions.
- (5) Given the normal stresses in the single-welded plates, it was observed that the in-plane stresses were low when welding speeds were between approximately 10-40 MPa. As welding speeds increased, the average in-plane stress across the plate also rose. In the multi-welded plates, adjacent welds contributed to complex stress interactions, generating normal non-zero tensile stresses of around 50 MPa, even in the base material. When comparing the multi-welded plates to the welded high-pressure die-cast components from the battery tray, normal (Z) residual stresses were significantly elevated to approximately 100 MPa. This increase is due to multiple factors, including the influence of adjacent welds and the possible inheritance of residual stresses from the high-pressure die-cast component before welding.

5.1 Contributions of Research

The research described in this dissertation significantly advances the understanding and optimization of friction stir welding (FSW) processes, especially in joining dissimilar aluminum alloys for automotive applications. The study offers valuable insights into microstructural changes,

residual stress evolution, and the effects of post-weld straightening techniques on the performance and durability of welded components. By employing advanced characterization methods such as neutron diffraction, electron backscatter diffraction (EBSD), and inductively coupled plasma atomic emission spectroscopy (ICP-AES), this research provides a comprehensive analysis of the factors influencing weld quality and mechanical properties. The findings are crucial for improving manufacturing processes and enhancing the structural integrity of lightweight automotive components, contributing to the efficiency and sustainability of the transportation industry.

The following list summarizes the novel contributions that originate from this research:

1. Characterization of FSW Microstructure and Residual Stress in the As-Welded Non-Straightened Battery Tray
 - The research investigates residual stress profiles from the FSW process, focusing on an industrial full-size high-pressure die-cast battery tray component. Analyzing a full-sized component rather than simplified samples provides realistic insights into residual stress distribution and its impact on structural performance and longevity. Neutron diffraction reveals how preheating by previous welds redistributes stresses in subsequent welds. This finding is confirmed through the analysis of simplified multi-welded plates, highlighting how initial weld-induced stresses are redistributed by subsequent welds.
2. Microstructural Changes in Friction Stir Welds of the Battery Tray
 - EBSD analysis quantifies plastic deformation and texture variations across the welds. At a constant speed ratio, grain size remains consistent, indicating that grain refinement strengthening does not account for hardness differences between welds. Increased rotational and traverse speeds lead to more significant plastic deformation and misorientation, which explains the higher Vickers hardness observed in faster welds.
 - ICP-AES analysis identifies variations in elemental concentrations, particularly silicon. While it was expected that higher rotational speeds might reduce silicon accumulation, the key finding is the clear segregation or accumulation of silicon within the welds. This supports the observation that stress-free results vary with solute content due to friction stir welding.

3. Analyzing Distortion and Stress Effects in Response to Straightening the Battery Tray
 - The study examines the impact of FSW on residual stress and distortion in battery trays, focusing on post-weld straightening techniques like burnishing and coining applied to multi-welded components. Connecting direct distortion measurements with post-straightening outcomes in multi-welded components is a relatively unexplored area. Burnishing induces compressive stresses to reduce tensile stresses, restoring pre-weld specifications, while coining focuses solely on achieving dimensional accuracy, sometimes increasing stresses in certain directions.
4. Residual Stress Characteristics in Friction Stir Welding: An In-depth Temperature, Texture, and Neutron Diffraction Analysis of Speed Parameters
 - Integrating multiple data collection methods, particularly Smoothed Particle Hydrodynamics (SPH), to model near-tool temperatures provides a more accurate approach compared to traditional finite element methods (FEM). This research connects SPH modeling with neutron diffraction and EBSD to provide a comprehensive analysis of residual stresses and texture changes at consistent speed ratios but varying rotational and traverse speeds. This approach reveals texture randomness and material behavior changes previously undocumented.
5. Influence of Multiple Welds on Residual Stress and Distortion in Friction Stir Welding: A Neutron Diffraction Study
 - The research highlights the impact of welding sequence and proximity of multiple welds on residual stress and distortion. Reducing the complexity of a full-sized battery tray helps identify stress inheritance in multi-welded configurations. Neutron diffraction measurements show that starting with peripheral welds followed by central welds can reduce high tensile stresses at the edges, emphasizing the critical role of thermal management. This study identifies that complex components, like battery trays, contribute to unique stress profiles not observed in simplified geometries, guiding better welding practices for complex structures.

This dissertation, from both an industry and academic perspective, significantly broadens the understanding of residual stress generation in FSW. It demonstrates that numerous factors contribute to residual stress, including mechanical properties, texture, temperature history, welding

sequence, and component geometry. This comprehensive approach encourages researchers to explore these factors and their interconnections. Ultimately, this work can lead to the development of a robust database of correlations, enhancing the ability to predict and manage residual stresses in multi-welded FSW operations.

5.2 Limitations of Research

Despite the contributions of this research, there are several limitations that must be acknowledged. These limitations are discussed here, with future recommendations provided in the next section to address these issues and expand on the research.

1. Single Sample Limitation

- Some of the samples characterized in this study were single samples, meaning that only one sample was created and measured using neutron diffraction. This limitation arises due to the scarcity and competitive nature of obtaining beam time for neutron diffraction. Consequently, the variability in results was not considered, given the time and resource constraints. This limitation is significant and contributes to the limited availability of similar research studies.

2. Absence of Fatigue Life Measurements

- Fatigue life measurements were not conducted in this study. Residual stresses are known to drive crack initiation and propagation, damaging the microstructure and contributing to the applied load during cycling. As most components are designed to operate below the yield strength, fatigue is a major cause of failure, with 90% of engineering components failing due to fatigue. The absence of fatigue life experiments is a significant limitation. This omission was primarily due to time constraints and the potential for substantial content addition to the dissertation. Future research should include fatigue life measurements to comprehensively understand the impact of residual stresses.

3. In-Operation Stresses

- In-operation stresses were not a primary focus of this study. Although axial or plunging forces and torque measurements were conducted on single-welded plates and multi-welded sections, the stresses experienced during actual operation were not thoroughly examined. This limitation affects the applicability of the results to

real-world conditions, where in-operation stresses play a critical role in the performance and longevity of components.

4. Clamping Variability

- The study did not vary the clamping configurations, which is a significant limitation. As demonstrated in the literature review (specifically Figure 29), clamping significantly affects residual stresses. The clamping techniques, forces, and methods used between the battery tray and single/multi-welded plates were different. This variability can heavily influence the residual stresses and plastic deformation outcomes. While clamping was kept constant within specific sections, the lack of variation limits the generalizability of the conclusions, as clamping configurations in friction stir welding research vary considerably and significantly impact distortion outcomes.

5. Tool Wear Considerations

- Tool wear was not considered in this study. As the tool wears, changes in its geometry can significantly affect the flow of material and the resulting texture. Only an optimal tool with minimal to no wear was used for all welding processes in this study. The study acknowledges that significant tool wear would likely alter the texture and flow stress conclusions, but this was not measured. Future research should investigate the effects of tool wear to provide a more comprehensive understanding of the welding process.

In summary, these limitations highlight areas where future research can build upon and address the gaps identified in this study. By acknowledging these limitations, continued research can improve the reliability and applicability of findings in the field of residual stress and material performance.

5.3 Future Recommendations

The research described throughout this dissertation provides clear evidence of various aspects, including proximity preheating of previous welds, friction stir welding texture effects on yield strength, and potential effects on residual stress. These findings contribute significantly to developing FSW technology for aluminum alloy dissimilar lap welds. However, further development can be achieved through additional research in several key areas, which include:

1. Reclassifying Friction Stir Welding in Context with Forming Processes

- Friction stir welding (FSW) should be reconsidered not just as a welding process but as a forming process. Unlike fusion welding, which results in cast-like structures with no significant texture other than that imparted by cooling, FSW shares similarities with extrusion, hot rolling, and other forming processes in developing unique textures. Fusion welding does not impart the same deforming, plasticizing, and recrystallizing textures seen in forming processes. Therefore, the comparison between FSW and fusion welding, which is common in the literature, may be less accurate. FSW should be studied more in relation to forming processes that impart texture, as it fundamentally has many forming-like aspects. This new classification and focused research on FSW textures will enhance our understanding of the process.

2. Utilizing Digital Image Correlation for Yield Strength Mapping

- A future recommendation is to use Digital Image Correlation (DIC) or create a specific rigging for the PIP machine to accurately map yield strength changes at multiple locations within the FSW. While this may be unrealistic for PIP, DIC has been successfully used in other studies. It is suggested to conduct DIC and EBSD texture analysis on the same material. Perform a large-scale EBSD map and then use DIC on the entire surface of the weld cross-section. Correlate the Schmid factor or other aligned grains with the yield strength mapping. If a strong correlation is found, as suggested in this dissertation, it will significantly enhance our understanding of deformation in FSW.

3. Analyzing Stress-Free Equivalent Comb Samples in Neutron Diffraction

- Another recommendation is to use equivalent comb samples for stress-free analysis in neutron diffraction, especially for welds or FSW that change the concentration from the base material to the weld. It would be beneficial to see how the concentration changes from one pin location to another on the comb and match this to the stress-free profile. This is essential due to ongoing debates among scientists and institutions about the necessity of multiple stress-free results across a weld. Fully defining the concentration of elements within the matrix at multiple points along the comb will provide valuable insights.

4. Investigating Proximity Welding Effects in FSW

- Further study on the proximity welding effects of FSW is highly recommended. Conducting systematic investigations on multiple components with increasing distances between welds will be extremely beneficial. Currently, literature lacks studies on this, with existing works either coincidentally placing welds near each other or performing multiple welds on top of each other. Suggested studies should involve one weld followed by a second weld at varying distances (e.g., 5mm, 10mm, 15mm, 20mm) and measure the residual stress and distortion for each case. This will help create standards regarding the interaction of residual stress fields and the potential minimization of distortion. Additionally, the distance of the weld from the edge of the component is a significant factor, especially in industrial applications like coolant trays and battery trays. Studies should also focus on edge welding and its impact on residual stress and distortion.

5. Continuation of SPH Simulation for Residual Stress Analysis

- Continuation of the SPH (Smooth Particle Hydrodynamics) simulation for residual stress analysis using this dataset is recommended for confirmation of the values. A collaborative project with METALtec and Dr. Fraser would be advantageous, as it would leverage this dataset to validate the simulation results. Given the multiple speed parameters utilized in the data collection, these simulations could provide a robust reference point for comparing simulation results with real-world neutron diffraction measurements. Implementing a texture model to accurately describe the effect of texture on yield strength could further enhance the understanding of residual stress retention in elasticity versus release into plasticity around the weld, advancing the knowledge of residual stress generation.
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6. In-Situ Residual Stress Measurements

- In-situ residual stress measurements to observe the development of residual stresses, their release into deformation, and continued generation after the end of the weld are recommended. This project, suggested by Dr. Arianna E. Gleason-Holbrook from Stanford during a visit to Los Alamos, could be conducted on specific beamlines. Although this project was beyond the scope of the current work,

it holds great promise for understanding the evolution of residual stresses during operation, which is crucial for comprehending their impact.

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7. Fatigue Testing

- Future research should incorporate fatigue testing to complement the residual stress analysis conducted in this study. While 6061 aluminum and its friction stir welded counterpart have established fatigue limits under standard conditions, the unique residual stress field from complex multi-welding to a cast component introduces uncertainties not covered by existing literature. Accurate fatigue analysis requires replicating real-world loading conditions precisely, necessitating a large-scale fatigue cycling rig for the entire battery tray component or a similar, albeit smaller, version for the plate geometry utilized in this study. Extracting smaller samples for fatigue testing would alter stress distributions, making results non-representative of the effects of residual stress on the component's fatigue life. Therefore, true fatigue testing should involve multiple full-scale components and industry-grade setups. By incorporating large-scale fatigue testing, future studies can provide a more comprehensive understanding of how residual stresses impact fatigue life, validate the residual stress analysis, and ultimately enhance the durability and performance of large multi-welded components.

These recommendations aim to further advance the understanding and application of friction stir welding, providing a framework for future research and standardization in the field.

References

- [1] N. Sabry, J. Stroh, and D. Sediako, "Characterization of microstructure and residual stress following the friction stir welding of dissimilar aluminum alloys," *CIRP J. Manuf. Sci. Technol.*, vol. 41, pp. 365–379, 2023, doi: 10.1016/j.cirpj.2022.11.021.
- [2] N. Sabry, J. Stroh, D. Sediako, G. Byczynski, A. Lombardi, and A. Payzant, "Stress characterization for friction-stir-welded electric vehicle battery trays with application of neutron diffraction," *J. Manuf. Process.*, vol. 101, no. March, pp. 1109–1123, 2023, doi: 10.1016/j.jmapro.2023.06.035.
- [3] N. Sabry, J. Stroh, and D. Sediako, "Effects of the Friction Stir Welding Sliding and Sticking Mechanisms on the Microhardness, Texture, and Element Concentration," *Light Met.* 2023, pp. 365–375, 2023, doi: 10.1007/978-3-031-22532-1.
- [4] T. Majeed, M. A. Wahid, M. N. Alam, Y. Mehta, and A. N. Siddiquee, "Friction stir welding: A sustainable manufacturing process," *Mater. Today Proc.*, vol. 46, no. xxxx, pp. 6558–6563, 2020, doi: 10.1016/j.matpr.2021.04.025.
- [5] M. Farhang, O. Sam-Daliri, M. Farahani, and A. Vatani, "Effect of friction stir welding parameters on the residual stress distribution of Al-2024-T6 alloy," *J. Mech. Eng. Sci.*, vol. 15, no. 1, pp. 7684–7694, 2021, doi: 10.15282/jmes.15.1.2021.06.0606.
- [6] D. G. Richards, P. B. Prangnell, P. J. Withers, S. W. Williams, T. Nagy, and S. Morgan, "Efficacy of active cooling for controlling residual stresses in friction stir welds," *Sci. Technol. Weld. Join.*, vol. 15, pp. 156–165, 2010, doi: 10.1179/136217109X12590746472490.
- [7] D. G. Richards, P. B. Prangnell, S. W. Williams, and P. J. Withers, "Global mechanical tensioning for the management of residual stresses in welds," *Mater. Sci. Eng. A*, vol. 489, no. 1–2, pp. 351–362, 2008, doi: 10.1016/j.msea.2007.12.042.
- [8] S. Delijaicov, P. A. de Oliveira Silva, H. B. Resende, and M. H. F. Batalha, "Effect of weld parameters on residual stress, hardness and microstructure of dissimilar AA2024-T3 and AA7475-T761 friction stir welded joints," *Mater. Res.*, vol. 21, pp. 1–11, 2018, doi: 10.1590/1980-5373-mr-2018-0108.
- [9] T. Zhang, Y. He, Q. Shao, H. Zhang, and L. Wu, "Comparative Study on fatigue properties of friction stir welding joint and lap joint," *13th Int. Conf. Fract. 2013, ICF 2013*, vol. 2, pp. 1604–1613, 2013.
- [10] L. N. Brewer, M. S. Bennett, B. W. Baker, E. A. Payzant, and L. M. Sochalski-Kolbus, "Characterization of residual stress as a function of friction stir welding parameters in oxide dispersion strengthened (ODS) steel MA956," *Mater. Sci. Eng. A*, vol. 647, pp. 313–321, 2015, doi: 10.1016/j.msea.2015.09.020.
- [11] M. Peel, A. Steuwer, M. Preuss, and P. J. Withers, "Microstructure, mechanical properties and residual stresses as a function of welding speed in aluminium AA5083 friction stir welds," *Acta Mater.*, vol. 51, no. 16, pp. 4791–4801, 2003, doi: 10.1016/S1359-6454(03)00319-7.

- [12] J. He, Z. Ling, and H. Li, "Effect of tool rotational speed on residual stress, microstructure, and tensile properties of friction stir welded 6061-T6 aluminum alloy thick plate," *Int. J. Adv. Manuf. Technol.*, vol. 84, no. 9–12, pp. 1953–1961, 2016, doi: 10.1007/s00170-015-7859-7.
- [13] M. Smith, J. B. Levesque, L. Bichler, D. Sediako, J. Gholipour, and P. Wanjara, "Residual stress analysis in linear friction welded in-service Inconel 718 superalloy via neutron diffraction and contour method approaches," *Mater. Sci. Eng. A*, vol. 691, no. February, pp. 168–179, 2017, doi: 10.1016/j.msea.2017.03.038.
- [14] A. Lombardi, D. Sediako, A. Machin, C. Ravindran, and R. MacKay, "Effect of solution heat treatment on residual stress in Al alloy engine blocks using neutron diffraction," *Mater. Sci. Eng. A*, vol. 697, no. May, pp. 238–247, 2017, doi: 10.1016/j.msea.2017.05.026.
- [15] D. Sediako *et al.*, "Analysis of Residual Stress Profiles in the Cylinder Web Region of an As-Cast V6 Al Engine Block with Cast-In Fe Liners Using Neutron Diffraction," *SAE Int. J. Mater. Manuf.*, vol. 4, no. 1, pp. 138–151, 2011, doi: 10.4271/2011-01-0036.
- [16] D. Sediako *et al.*, "Application of neutron diffraction in analysis of residual stress profiles in the cylinder web region of an as-cast v6 ai engine block with cast-in fe liners," *SAE Tech. Pap.*, vol. 2, pp. 299–308, 2011.
- [17] O. Engler and J. Hirsch, "Texture control by thermomechanical processing of AA6xxx Al-Mg-Si sheet alloys for automotive applications - a review," *Mater. Sci. Eng. A*, vol. 336, no. 1–2, pp. 249–262, 2002, doi: 10.1016/S0921-5093(01)01968-2.
- [18] H. R. Shercliff and M. F. Ashby, "A process model for age hardening of aluminium alloys-I. The model," *Acta Metall. Mater.*, vol. 38, no. 10, pp. 1789–1802, 1990, doi: 10.1016/0956-7151(90)90291-N.
- [19] C. Sharma and V. Upadhyay, "Friction Stir Welding of Dissimilar Aluminum Alloys AA5086 and AA7039," *J. Phys. Conf. Ser.*, vol. 1240, no. 1, pp. 1–8, 2019, doi: 10.1088/1742-6596/1240/1/012160.
- [20] T. DebRoy and H. K. D. H. Bhadeshia, "Friction stir welding of dissimilar alloys - A perspective," *Sci. Technol. Weld. Join.*, vol. 15, no. 4, pp. 266–270, 2010, doi: 10.1179/174329310X12726496072400.
- [21] S. M. O. Tavares, R. A. S. Castro, V. Richter-Trummer, P. Vilaça, P. M. G. P. Moreira, and P. M. S. T. De Castro, "Friction stir welding of T-joints with dissimilar aluminium alloys: Mechanical joint characterisation," *Sci. Technol. Weld. Join.*, vol. 15, no. 4, pp. 312–318, 2010, doi: 10.1179/136217109X12562846839114.
- [22] M. J. Peel, A. Steuwer, and P. J. Withers, "Dissimilar friction stir welds in AA5083-AA6082. Part II: Process parameter effects on microstructure," *Metall. Mater. Trans. A Phys. Metall. Mater. Sci.*, vol. 37, no. 7, pp. 2195–2206, 2006, doi: 10.1007/BF02586139.
- [23] M. Warmuzek, *Aluminum-silicon casting alloys: An Atlas of Microfractographs*. ASM International(OH), 2004.
- [24] L. Wang, M. Makhlof, and D. Apelian, "Aluminium die casting alloys: Alloy

- composition, microstructure, and properties-performance relationships,” *Int. Mater. Rev.*, vol. 40, pp. 221–238, 1995, doi: 10.1179/imr.1995.40.6.221.
- [25] Y. Birol, “Semi-solid processing of the primary aluminium die casting alloy A365,” *J. Alloys Compd.*, vol. 473, no. 1–2, pp. 133–138, 2009, doi: 10.1016/j.jallcom.2008.05.074.
 - [26] S. Ekambaram and N. Murugan, “Synthesis and Characterization of Aluminium Alloy AA6061-Alumina Metal Matrix Composite International Journal of Current Engineering and Technology Synthesis and Characterization of Aluminium Alloy AA6061-Alumina Metal Matrix Composite,” *Artic. Int. J. Eng. Technol.*, vol. 5, no. 5, pp. 3211–3216, 2015, [Online]. Available: <http://inpressco.com/category/ijcet>
 - [27] C. Y. Jeong, “High temperature mechanical properties of AlSiMg(Cu) alloys for automotive cylinder heads,” *Mater. Trans.*, vol. 54, no. 4, pp. 588–594, 2013, doi: 10.2320/matertrans.M2012285.
 - [28] P. Hidnert and H. S. Krider, “Thermal expansion of aluminum and some aluminum alloys,” *J. Res. Natl. Bur. Stand. (1934).*, vol. 48, p. 209, 1952, doi: 10.6028/jres.048.030.
 - [29] L. E. Murr, “A review of FSW research on dissimilar metal and alloy systems,” *J. Mater. Eng. Perform.*, vol. 19, no. 8, pp. 1071–1089, 2010, doi: 10.1007/s11665-010-9598-0.
 - [30] P. H. Shah and V. J. Badheka, *Friction stir welding of aluminium alloys: An overview of experimental findings – process, variables, development and applications*, vol. 233, no. 6. 2019. doi: 10.1177/1464420716689588.
 - [31] A. Simar, Y. Bréchet, B. De Meester, A. Denquin, C. Gallais, and T. Pardoen, “Integrated modeling of friction stir welding of 6xxx series Al alloys: Process, microstructure and properties,” *Prog. Mater. Sci.*, vol. 57, no. 1, pp. 95–183, 2012, doi: 10.1016/j.pmatsci.2011.05.003.
 - [32] E. Sharghi and A. Farzadi, “Simulation of temperature distribution and heat generation during dissimilar friction stir welding of AA6061 aluminum alloy and Al-Mg₂Si composite,” *Int. J. Adv. Manuf. Technol.*, vol. 118, no. 9–10, pp. 3147–3159, 2022, doi: 10.1007/s00170-021-08175-z.
 - [33] L. Wang, C. M. Davies, R. C. Wimpory, L. Y. Xie, and K. M. Nikbin, “Measurement and simulation of temperature and residual stress distributions from friction stir welding AA2024 Al alloy,” *Mater. High Temp.*, vol. 27, no. 3, pp. 167–178, 2010, doi: 10.3184/096034010X12813743510192.
 - [34] A. Silva-Magalhães, J. De Backer, J. Martin, and G. Bolmsjö, “In-situ temperature measurement in friction stir welding of thick section aluminium alloys,” *J. Manuf. Process.*, vol. 39, no. February 2018, pp. 12–17, 2019, doi: 10.1016/j.jmapro.2019.02.001.
 - [35] K. Fraser, L. I. Kiss, L. St-Georges, and D. Drolet, “Optimization of friction stir weld joint quality using a meshfree fully-coupled thermo-mechanics approach,” *Metals (Basel).*, vol. 8, no. 2, 2018, doi: 10.3390/met8020101.
 - [36] P. L. Threadgill, A. J. Leonard, H. R. Shercliff, and P. J. Withers, “Friction stir welding of aluminium alloys,” *Int. Mater. Rev.*, vol. 54, no. 2, pp. 49–93, 2009, doi: 10.1179/174328009X411136.

- [37] D. A. P. Prabhakar *et al.*, “A Review of Optimization and Measurement Techniques of the Friction Stir Welding (FSW) Process,” *J. Manuf. Mater. Process.*, vol. 7, no. 5, 2023, doi: 10.3390/jmmp7050181.
- [38] M. M. El-Sayed, A. Y. Shash, M. Abd-Rabou, and M. G. ElSherbiny, “Welding and processing of metallic materials by using friction stir technique: A review,” *J. Adv. Join. Process.*, vol. 3, pp. 1–20, 2021, doi: 10.1016/j.jajp.2021.100059.
- [39] M. J. Peel, A. Steuwer, P. J. Withers, T. Dickerson, Q. Shi, and H. Shercliff, “Dissimilar friction stir welds in AA5083-AA6082. Part I: Process parameter effects on thermal history and weld properties,” *Metall. Mater. Trans. A Phys. Metall. Mater. Sci.*, vol. 37, no. 7, pp. 2183–2193, 2006, doi: 10.1007/BF02586138.
- [40] A. J. Schwartz, M. Kumar, B. L. Adams, and D. P. Field, *Electron Backscatter Diffraction in Materials Science*, vol. 86, no. 1. 2009. [Online]. Available: <http://dx.doi.org/10.1016/j.actamat.2012>
- [41] X. Geng *et al.*, “Microstructure, mechanical and corrosion properties of friction stirwelding high nitrogen martensitic stainless steel 30Cr15Mo1N,” *Metals (Basel)*., vol. 6, no. 12, pp. 1–14, 2016, doi: 10.3390/met6120301.
- [42] R. S. Mishra and Z. Y. Ma, *Friction stir welding and processing*, vol. 50, no. 1–2. 2005. doi: 10.1016/j.msar.2005.07.001.
- [43] A. S. Sedmak *et al.*, “Heat input effect of friction stir welding on aluminum alloy AA 6061-T6 welded joint,” *Therm. Sci.*, vol. 20, no. 2, pp. 637–641, 2016, doi: 10.2298/TSCI150814147D.
- [44] A. S. Golezani, R. V. Barenji, A. Heidarzadeh, and H. Pouraliakbar, “Elucidating of tool rotational speed in friction stir welding of 7020-T6 aluminum alloy,” *Int. J. Adv. Manuf. Technol.*, vol. 81, no. 5–8, pp. 1155–1164, 2015, doi: 10.1007/s00170-015-7252-6.
- [45] W. Arbegast and P. Hartley, “Friction stir weld Technology Development at Lockheed Martin Michoud Space System-An Overview,” in *Trends in welding research : proceedings of the 5th international conference*, 1998.
- [46] K. J. Colligan and R. S. Mishra, “A conceptual model for the process variables related to heat generation in friction stir welding of aluminum,” *Scr. Mater.*, vol. 58, no. 5, pp. 327–331, 2008, doi: 10.1016/j.scriptamat.2007.10.015.
- [47] C. G. Rhodes, M. W. Mahoney, W. H. Bingel, R. A. Spurling, and C. C. Bampton, “Effects of friction stir welding on microstructure of 7075 aluminum,” *Scr. Mater.*, vol. 36, no. 1, pp. 69–75, 1997, doi: 10.1016/S1359-6462(96)00344-2.
- [48] E. Aldanondo, J. Vivas, P. Álvarez, and I. Hurtado, “Effect of tool geometry and welding parameters on friction stir welded lap joint formation with aa2099-t83 and aa2060-t8e30 aluminium alloys,” *Metals (Basel)*., vol. 10, no. 7, pp. 1–12, 2020, doi: 10.3390/met10070872.
- [49] A. P. Reynolds, “Visualisation of material flow in autogenous friction stir welds,” *Sci. Technol. Weld. Join.*, vol. 5, no. 2, pp. 120–124, 2000, doi: 10.1179/136217100101538119.

- [50] J. Q. Su, T. W. Nelson, R. Mishra, and M. Mahoney, "Microstructural investigation of friction stir welded 7050-T651 aluminium," *Acta Mater.*, vol. 51, no. 3, pp. 713–729, 2003, doi: 10.1016/S1359-6454(02)00449-4.
- [51] S. Benavides, Y. Li, L. E. Murr, D. Brown, and J. C. McClure, "Low-temperature friction-stir welding of 2024 aluminum," *Scr. Mater.*, vol. 41, no. 8, pp. 809–815, 1999, doi: 10.1016/S1359-6462(99)00226-2.
- [52] P. T. V Satyavallabh, S. Kumar, and P. Saraswathi, "Effect of Weld Parameters of Friction Stir Welding on Grain Size and Surface Roughness of Welded Joint of AA8011 and AA6061 Aluminium Alloy," vol. 5, no. 10, pp. 78–84, 2018.
- [53] R. S. Mishra, P. S. De, and N. Kumar, *Friction Stir Welding and Processing*. New York: Springer, 2014. doi: 10.1007/978-3-319-07043-8.
- [54] Y. F. Wu, J. Yu, Z. Yang, and Y. L. Deng, "Study of grain distribution during friction stir welding of Al-Zn-Mg alloys using numerical simulation," *IOP Conf. Ser. Mater. Sci. Eng.*, vol. 668, no. 1, pp. 0–12, 2019, doi: 10.1088/1757-899X/668/1/012022.
- [55] J. Purkiss, *Fire Safety Engineering Design of Structures*. 2007. doi: 10.1201/b12845.
- [56] F. Fadaeifard, K. A. Matori, S. Abd Aziz, L. Zolkarnain, and M. A. Z. Bin Abdul Rahim, "Effect of the welding speed on the macrostructure, microstructure and mechanical properties of AA6061-T6 friction stir butt welds," *Metals (Basel)*, vol. 7, no. 2, 2017, doi: 10.3390/met7020048.
- [57] L. Liu, H. Nakayama, S. Fukumoto, A. Yamamoto, and H. Tsubakino, "Microscopic Observations of Friction Stir Welded 6061 Aluminum Alloy," *Mater. Trans.*, vol. 45, no. 2, pp. 288–291, 2004, doi: 10.2320/matertrans.45.288.
- [58] H. J. K. Lemmen, R. C. Alderliesten, R. R. G. M. Pieters, R. Benedictus, and J. A. Pineault, "Yield strength and residual stress measurements on friction-stir-welded aluminum alloys," *J. Aircr.*, vol. 47, pp. 1570–1583, 2010, doi: 10.2514/1.C000212.
- [59] C. Zhang and L. S. Toth, "Polycrystal simulation of texture-induced grain coarsening during severe plastic deformation," *Materials (Basel)*, vol. 13, no. 24, pp. 1–16, 2020, doi: 10.3390/ma13245834.
- [60] J. Xie, X. P. Chen, Y. Cao, Y. B. Sun, G. J. Huang, and Q. Liu, "The heterogeneity of dynamic recrystallization and texture evolution in stirring zone of Al-Mg-Sc alloy," *Mater. Charact.*, vol. 191, no. March, p. 112092, 2022, doi: 10.1016/j.matchar.2022.112092.
- [61] R. W. Armstrong, "Hall–Petch Relationship in Aluminum and Aluminum Alloys," *Encycl. Alum. Its Alloy.*, pp. 1104–1122, 2019, doi: 10.1201/9781351045636-140000234.
- [62] W. Woo, H. Choo, M. B. Prime, Z. Feng, and B. Clausen, "Microstructure, texture and residual stress in a friction-stir-processed AZ31B magnesium alloy," *Acta Mater.*, vol. 56, no. 8, pp. 1701–1711, 2008, doi: 10.1016/j.actamat.2007.12.020.
- [63] E. Schmid and W. Boas, *Plasticity of Crystals With Special Reference to Metals*. London, 1950. doi: 10.1063/1.1750730.

- [64] B. Ennis, “A review of the effects of chemical and phase segregation on the mechanical behaviour of multi-phase steels,” no. April, 2016, [Online]. Available: <http://arxiv.org/abs/1604.06485>
- [65] F. C. Salvado, F. Teixeira-Dias, S. M. Walley, L. J. Lea, and J. B. Cardoso, “A review on the strain rate dependency of the dynamic viscoplastic response of FCC metals,” *Prog. Mater. Sci.*, vol. 88, pp. 186–231, 2017, doi: 10.1016/j.pmatsci.2017.04.004.
- [66] M. Hakem, S. Lebaili, S. Mathieu, D. Miroud, A. Lebaili, and B. Cheniti, “Effect of microstructure and precipitation phenomena on the mechanical behavior of AA6061-T6 aluminum alloy weld,” *Int. J. Adv. Manuf. Technol.*, vol. 102, no. 9–12, pp. 2907–2918, 2019, doi: 10.1007/s00170-019-03401-1.
- [67] G. Liu, L. E. Murr, C. S. Niou, J. C. McClure, and F. R. Vega, “Microstructural aspects of the friction-stir welding of 6061-T6 aluminum,” *Scr. Mater.*, vol. 37, no. 3, pp. 355–361, 1997, doi: 10.1016/S1359-6462(97)00093-6.
- [68] Y. S. Sato, H. Kokawa, M. Enomoto, S. Jogan, and T. Hashimoto, “Precipitation sequence in friction stir weld of 6063 aluminum during aging,” *Metall. Mater. Trans. A Phys. Metall. Mater. Sci.*, vol. 30, no. 12, pp. 3125–3130, 1999, doi: 10.1007/s11661-999-0223-5.
- [69] S. Mironov, Y. S. Sato, and H. Kokawa, “Development of grain structure during friction stir welding of pure titanium,” *Acta Mater.*, vol. 57, no. 15, pp. 4519–4528, 2009, doi: 10.1016/j.actamat.2009.06.020.
- [70] J. Xie, X. P. Chen, Y. Cao, G. J. Huang, and Q. Liu, “Analyzing time series of serrated flow and the dependence on structural heterogeneity in Al-Mg-Sc alloy,” *Mater. Des.*, vol. 221, p. 111006, 2022, doi: 10.1016/j.matdes.2022.111006.
- [71] P. J. Withers and H. K. D. H. Bhadeshia, “Residual stress part 1 - Measurement techniques,” *Mater. Sci. Technol.*, vol. 17, no. 4, pp. 355–365, 2001, doi: 10.1179/026708301101509980.
- [72] P. J. Withers and H. K. D. H. Bhadeshia, “Residual stress part 2 - Nature and origins,” *Mater. Sci. Technol.*, vol. 17, no. 4, pp. 366–375, 2001, doi: 10.1179/026708301101510087.
- [73] L. Daniel, D. A. Hall, and P. J. Withers, “A multiscale modelling analysis of the contribution of crystalline elastic anisotropy to intergranular stresses in ferroelectric materials,” *J. Phys. D. Appl. Phys.*, vol. 47, no. 32, 2014, doi: 10.1088/0022-3727/47/32/325303.
- [74] J. M. Gordo and G. Teixeira, “A simplified method to simulate residual stresses in plates,” *Dev. Marit. Technol. Eng.*, no. September, pp. 785–792, 2021, doi: 10.1201/9781003216582-89.
- [75] R. Sokkalingam, V. Muthupandi, K. Sivaprasad, and K. G. Prashanth, “Dissimilar welding of Al0.1CoCrFeNi high-entropy alloy and AISI304 stainless steel,” *J. Mater. Res.*, vol. 34, no. 15, pp. 2683–2694, 2019, doi: 10.1557/jmr.2019.186.
- [76] J. Woodtli, W. Muster, and J. C. Radon, “Residual stress effects in fatigue crack growth,”

- Eng. Fract. Mech.*, vol. 24, no. 3, pp. 399–412, 1986, doi: 10.1016/0013-7944(86)90070-6.
- [77] D. A. Price *et al.*, “Distortion control in welding by mechanical tensioning,” *Sci. Technol. Weld. Join.*, vol. 12, no. 7, pp. 620–633, 2007, doi: 10.1179/174329307X213864.
 - [78] Y. Li, J. Zhao, J. Zhou, Y. Yang, X. Huang, and Z. Liu, “Evaluation of Residual Stress Fields in Friction Stir Welded Zone Based on the Plastic Strain Increment and Mises Yield Criterion,” *J. Mater. Eng. Perform.*, vol. 32, no. 12, pp. 5466–5474, 2023, doi: 10.1007/s11665-022-07477-x.
 - [79] X. Liu *et al.*, “Residual stress, microstructure and mechanical properties in thick 6005a-t6 aluminium alloy friction stirwelds,” *Metals (Basel)*, vol. 9, no. 7, 2019, doi: 10.3390/met9070803.
 - [80] K. Masubuchi, *Analysis and Control of Residual Stresses, Distortion and Their Consequences in Welded Structures*. London: Pergamon, 1982.
 - [81] E. M. Van der Aa, “Local cooling during welding: Prediction and control of residual stresses and buckling distortion,” 2007.
 - [82] S. W. Williams and A. Steuwer, *Residual stresses in friction stir welding*. Woodhead Publishing Limited, 2009. doi: 10.1533/9781845697716.2.215.
 - [83] M. H. El-Moayed, A. Y. Shash, M. A. Rabou, and M. G. El-Sherbiny, “Thermal-induced Residual Stresses and Distortions in Friction Stir Welds - A Literature Review,” *J. Weld. Join.*, vol. 39, no. 4, pp. 409–418, 2021, doi: 10.5781/jwj.2021.39.4.9.
 - [84] S. W. Wen, P. A. Colegrove, S. W. Williams, S. A. Morgan, A. Wescott, and M. Poad, “Rolling to control residual stress and distortion in friction stir welds,” *Sci. Technol. Weld. Join.*, vol. 15, no. 6, pp. 440–447, 2010, doi: 10.1179/136217110X12785889549787.
 - [85] J. Altenkirch, “Stress engineering of Friction Stir Welding: Measurement and Control of Welding Residual Stresses,” University of Manchester, 2009.
 - [86] H. Yu, X. Xu, B. Zheng, and X. Lai, “Welding-induced buckling prediction for large thin-walled cylindrical structures with non-uniform stress fields by friction stir welding,” *Int. J. Adv. Manuf. Technol.*, vol. 103, no. 9–12, pp. 4635–4647, 2019, doi: 10.1007/s00170-019-03929-2.
 - [87] H. Porzner, “Possibilities of Numerical Simulation for Evaluation and Optimisation of Welded Designs,” *Math. Model. Weld Phenom.* 5, 2001.
 - [88] P. Staron, M. Koçak, and S. Williams, “Residual stresses for friction stir welded Al sheets,” *Appl. Phys. A Mater. Sci. Process.*, vol. 28, no. 6, pp. 105–108, 2007.
 - [89] W. T. Han, F. R. Wan, G. Li, C. L. Dong, and J. H. Tong, “Effect of trailing heat sink on residual stresses and welding distortion in friction stir welding al sheets,” *Sci. Technol. Weld. Join.*, vol. 16, no. 5, pp. 453–458, 2011, doi: 10.1179/1362171811Y.0000000015.
 - [90] P. Staron, M. Koçak, S. Williams, and A. Wescott, “Residual stress in friction stir-welded Al sheets,” *Phys. B Condens. Matter*, vol. 350, no. 1-3 SUPPL. 1, pp. 491–493, 2004, doi:

10.1016/j.physb.2004.03.128.

- [91] J. Altenkirch, A. Steuwer, M. Peel, D. G. Richards, and P. J. Withers, “The effect of tensioning and sectioning on residual stresses in aluminium AA7749 friction stir welds,” *Mater. Sci. Eng. A*, vol. 488, no. 1–2, pp. 16–24, 2008, doi: 10.1016/j.msea.2007.10.055.
- [92] J. Altenkirch, A. Steuwer, P. J. Withers, S. W. Williams, M. Poad, and S. W. Wen, “Residual stress engineering in friction stir welds by roller tensioning,” *Sci. Technol. Weld. Join.*, vol. 14, pp. 185–192, 2009, doi: 10.1179/136217108X388624.
- [93] D. Park, J. Park, and N. Kim, “A Method for Straightening Distorted Giga-Cast Large Thin-Walled Components,” *Materials (Basel)*, vol. 17, no. 10, p. 2241, 2024, doi: 10.3390/ma17102241.
- [94] H. Başak, S. Özkan, and A. Taşkesen, “Application of burnishing process on friction stir welding and investigation of the effect of burnishing process on the surface roughness, hardness and strength,” *Exp. Tech.*, vol. 35, no. 1, pp. 8–16, 2011, doi: 10.1111/j.1747-1567.2009.00555.x.
- [95] R. Kluz, M. Bucior, A. Dzierwa, K. Antosz, W. Bochnowski, and K. Ochał, “Effect of Diamond Burnishing on the Properties of FSW Joints of EN AW-2024 Aluminum Alloys,” *Appl. Sci.*, vol. 13, no. 3, 2023, doi: 10.3390/app13031305.
- [96] P. S. Prev y and F. Lane, “The influence of surface enhancement by low plasticity on the corrosion fatigue performance of AA7075-T6,” *J. Fatigue*, vol. 26, no. 9, p. 975, 2004, [Online]. Available: <http://dx.doi.org/10.1016/j.ijfatigue.2004.01.010>
- [97] A. Rodr guez *et al.*, “Burnishing of FSW Aluminum Al-Cu-Li components,” *Metals (Basel)*, vol. 9, no. 2, pp. 1–16, 2019, doi: 10.3390/met9020260.
- [98] I. Ovali and A. Akkurt, “Comparison of burnishing process with other methods of hole surface finishing processes applied on brass materials,” *Mater. Manuf. Process.*, vol. 26, no. 8, pp. 1064–1072, 2011, doi: 10.1080/10426914.2010.536931.
- [99] F. J. Shiou and C. H. Chuang, “Precision surface finish of the mold steel PDS5 using an innovative ball burnishing tool embedded with a load cell,” *Precis. Eng.*, vol. 34, no. 1, pp. 76–84, 2010, doi: 10.1016/j.precisioneng.2009.03.003.
- [100] O. Hatamleh, “A comprehensive investigation on the effects of laser and shot peening on fatigue crack growth in friction stir welded AA 2195 joints,” *Int. J. Fatigue*, vol. 31, no. 5, pp. 974–988, 2009, doi: 10.1016/j.ijfatigue.2008.03.029.
- [101] Y. Javadi, S. Sadeghi, and M. A. Najafabadi, “Taguchi optimization and ultrasonic measurement of residual stresses in the friction stir welding,” *Mater. Des.*, vol. 55, pp. 27–34, 2014, doi: 10.1016/j.matdes.2013.10.021.
- [102] M. Ya, F. Dai, H. Xie, and J. Lu, “Measurement of non-uniform residual stresses by combined moir  interferometry and hole-drilling method: Theory, experimental method and applications,” *Acta Mech. Sin. Xuebao*, vol. 19, no. 6, pp. 567–574, 2003, doi: 10.1007/bf02484550.
- [103] V. Dattoma, M. De Giorgi, and R. Nobile, “On the residual stress field in the aluminium

- alloy FSW joints,” *Strain*, vol. 45, no. 4, pp. 380–386, 2009, doi: 10.1111/j.1475-1305.2008.00415.x.
- [104] J. Mathar, “Determination of Initial Stresses by Measuring the Deformation Around Drilled Holes,” *Trans. ASME*, vol. 56, no. 1, pp. 249–254, 1934.
 - [105] A. Bandyopadhyay, G. T. Fry, and B. Watanabe, “Residual Stress Estimation Using Hole Drilling and Contour Methods in Rail Welds Treated with and without Ultrasonic Impact Treatment,” *J. Mater. Eng. Perform.*, no. Ref 1, 2024, doi: 10.1007/s11665-024-09665-3.
 - [106] ASTM E837, “E837-20 - Standard Test Method for Determining Residual Stresses by the Hole-Drilling Strain-Gages,” 2020. doi: 10.1520/E0837-13A.2.
 - [107] Vishay Precision, “Measurement of Residual Stresses by the Hole-Drilling Strain Gage Method, Tech Note TN-503,” *Micro Meas.*, 2007.
 - [108] G. S. Schajer and P. S. Whitehead, *Hole-Drilling Method for Measuring Residual Stresses*. 2018. [Online]. Available: <http://link.springer.com/10.1007/978-3-319-59379-1%0Ahttp://dx.doi.org/10.1016/B978-0-12-420070-8.00002-7%0Ahttp://dx.doi.org/10.1016/j.ab.2015.03.024%0Ahttps://doi.org/10.1080/07352689.2018.1441103%0Ahttp://www.chile.bmw-motorrad.cl/sync/showroom/lam/es/>
 - [109] Y.-M. Chang, “Residual stress measurement by X Ray diffraction,” The University of Arizona, 1972.
 - [110] J. Zapata, M. Toro, and D. López, “Residual stresses in friction stir dissimilar welding of aluminum alloys,” *J. Mater. Process. Technol.*, vol. 229, pp. 121–127, 2016, doi: 10.1016/j.jmatprotec.2015.08.026.
 - [111] M. B. Prime, T. Gnäupel-Herold, J. A. Baumann, R. J. Lederich, D. M. Bowden, and R. J. Sebring, “Residual stress measurements in a thick, dissimilar aluminum alloy friction stir weld,” *Acta Mater.*, vol. 54, no. 15, pp. 4013–4021, 2006, doi: 10.1016/j.actamat.2006.04.034.
 - [112] B. D. Cullity and S. R. Stock, *Elements of X-Ray diffraction: Pearson New International Edition*. Pearson, 2013. doi: 10.1016/0014-5793(79)80596-7.
 - [113] B. E. Warren, *X-Ray diffraction*. Courier Corporation, 1990.
 - [114] P. Ji, Z. Yang, J. Zhang, L. Zheng, V. Ji, and V. Klosek, “Residual stress distribution and microstructure in the friction stir weld of 7075 aluminum alloy,” *J. Mater. Sci.*, vol. 50, no. 22, pp. 7262–7270, 2015, doi: 10.1007/s10853-015-9280-x.
 - [115] M. T. Hutchings, P. J. Withers, T. M. Holden, and T. Lorentzen, *Introduction to the Characterization of Residual Stress by Neutron Diffraction*. Boca Raton: CRC Press, 2005.
 - [116] R. Pynn, “Neutron Scattering, a Primer.” Los Alamos Science, 1990.
 - [117] F. a Kandil, J. D. Lord, a T. Fry, and P. V Grant, “A review of residual stress measurement methods - A guide to technical selection,” *NPL Mater. Cent.*, no. Report MATC(A)04, pp. 1–42, 2001.

- [118] D. Jacobson, M. Arif, P. Huffman, and R. Satija, “A New Neutron Imaging Facility at BT-6 for the Non-Destructive Analysis of Working Fuel Cells,” *NCNR Annu. Rep. 2003*, vol. NIST Speci, no. October, pp. 8–9, 2003.
- [119] S. Gao, “A combined theoretical and experimental analysis on performance and functionality of printed dielectric mirrors,” 2019.
- [120] J. Als-Nielsen and D. McMorrow, *Elements of Modern X-ray Physics*, Second Edi., vol. 55, no. 3. New York: Wiley, 2011. doi: 10.1063/1.1472397.
- [121] Britannica, “Bragg law,” *The Editors of Encyclopaedia*, 2022.
<https://www.britannica.com/science/Bragg-law>
- [122] J. R. Connolly, *Introduction to X-Ray Powder Diffraction*. 2007.
- [123] “Non-destructive testing — Standard test method for determining residual stresses by neutron diffraction,” *ISO/TS 21432:2005*, 2005.
- [124] G. S. Schajer, *Practical Residual Stress Measurement Methods*. 2013.
- [125] I. C. Noyan and J. B. Cohan, *Residual Stress - Measurement by Diffraction and Interpolation*. 1987. [Online]. Available: <http://link.springer.com/10.1007/978-1-4613-9570-6>
- [126] V. K. Pecharsky and P. Y. Zavalij, *Fundamentals of Powder Diffraction and Structural Characterization of Materials*. Boston: Springer US, 2008.
- [127] P. J. Webster, “Spatial Resolution and Strain Scanning,” *Meas. Residual Appl. Stress Using Neutron Diffr.*, pp. 235–251, 1992, doi: 10.1007/978-94-011-2797-4_15.
- [128] T. Lorentzen and T. Leffers, “Strain Tensor Measurements by Neutron Diffraction,” *Meas. Residual Appl. Stress Using Neutron Diffr.*, pp. 253–261, 1992, doi: 10.1007/978-94-011-2797-4_16.
- [129] M. T. Hutchings and A. D. Krawitz, *Measurement of residual and applied stress using neutron diffraction*. Springer. doi: <https://doi.org/10.1007/978-94-011-2797-4>.
- [130] H. G. Priesmeyer, “The stress-free reference sample: alloy composmon information from neutron capture,” *Meas. Residual Appl. Stress Using Neutron Diffr.*, pp. 277–284, 1992.
- [131] C. Barrett and T. B. M. Assalski, *Structure of Metals: Crystallographic Methods, Principles and Data*, 3rd ed. Pergamon.
- [132] M. Hutchings and A. D. Krawitz, *Measurement of residual and applied stress using neutron diffraction*. 1992. doi: 10.1007/978-94-011-2797-4_46.
- [133] S. Ganguly, L. Edwards, and M. E. Fitzpatrick, “Problems in using a comb sample as a stress-free reference for the determination of welding residual stress by diffraction,” *Mater. Sci. Eng. A*, vol. 528, no. 3, pp. 1226–1232, 2011, doi: 10.1016/j.msea.2010.10.039.
- [134] A. Paradowska, T. R. Finlayson, J. W. H. Price, R. Ibrahim, A. Steuwer, and M. Ripley, “Investigation of reference samples for residual strain measurements in a welded

- specimen by neutron and synchrotron X-ray diffraction,” *Phys. B Condens. Matter*, vol. 385–386, pp. 904–907, 2006, doi: 10.1016/j.physb.2006.05.245.
- [135] T. Liu *et al.*, “Neutron Diffraction Analysis of Residual Strain in High-Pressure Die Cast A383 Engine Blocks,” *J. Mater. Eng. Perform.*, vol. 29, pp. 5428–5434, 2020, doi: 10.1007/s11665-020-05019-x.
- [136] A. Ul-Hamid, *A Beginners’ Guide to Scanning Electron Microscopy*. 2018. doi: 10.1007/978-3-319-98482-7.
- [137] M. A. Sutton, N. Li, D. C. Joy, A. P. Reynolds, and X. Li, “Scanning electron microscopy for quantitative small and large deformation measurements Part I: SEM imaging at magnifications from 200 to 10,000,” *Exp. Mech.*, vol. 47, no. 6, pp. 775–787, 2007, doi: 10.1007/s11340-007-9042-z.
- [138] B. Fultz and J. Howe, *Transmission electron microscopy and diffractometry of materials*, vol. 6, no. 1. Springer Science & Business Media., 2012.
- [139] R. O. Duda and P. E. Hart, “Use of the Hough Transformation to Detect Lines and Curves in Pictures,” *Commun. ACM*, vol. 15, no. 1, pp. 11–15, 1972, doi: 10.1145/361237.361242.
- [140] S. I. Wright and B. L. Adams, “Automatic analysis of electron backscatter diffraction patterns,” *Metall. Trans. A*, vol. 23, no. 3, pp. 759–767, 1992, doi: 10.1007/BF02675553.
- [141] D. Bouscaud *et al.*, “Hétérogénéités de contraintes intragranulaires : détermination par approche couplée EBSD-Kossel To cite this version : HAL Id : hal-03422537 Hétérogénéités de contraintes intragranulaires : Détermination par approche couplée EBSD-Kossel Résumé : Abstract ,” 2021.
- [142] A. D. Herron, S. P. Coleman, K. Q. Dang, D. E. Spearot, and E. R. Homer, “Simulation of kinematic Kikuchi diffraction patterns from atomistic structures,” *MethodsX*, vol. 5, no. September, pp. 1187–1203, 2018, doi: 10.1016/j.mex.2018.09.001.
- [143] A. Kar, S. Suwas, and S. V. Kailas, “Two-pass friction stir welding of aluminum alloy to titanium alloy: A simultaneous improvement in mechanical properties,” *Mater. Sci. Eng. A*, vol. 733, no. July, pp. 199–210, 2018, doi: 10.1016/j.msea.2018.07.057.
- [144] V. Patel *et al.*, “High-speed friction stir welding in light weight battery trays for the EV industry,” *Sci. Technol. Weld. Join.*, vol. 27, no. 4, pp. 250–255, 2022, doi: 10.1080/13621718.2022.2045121.
- [145] M. Nourani, A. S. Milani, and S. Yannacopoulos, “On experimental optimization of friction stir welding of aluminum 6061: understanding processing-microstructure-property relations,” *Int. J. Adv. Manuf. Technol.*, vol. 79, no. 9–12, pp. 1425–1441, 2015, doi: 10.1007/s00170-015-6932-6.
- [146] M. M. Moradi, H. Jamshidi Aval, R. Jamaati, S. Amirkhanlou, and S. Ji, “Microstructure and texture evolution of friction stir welded dissimilar aluminum alloys: AA2024 and AA6061,” *J. Manuf. Process.*, vol. 32, pp. 1–10, 2018, doi: 10.1016/j.jmapro.2018.01.016.
- [147] G. T. Kridli, P. A. Friedman, and J. M. Boileau, “Manufacturing processes for light

- alloys,” *Mater. Des. Manuf. Light. Veh.*, pp. 267–320, 2020, doi: 10.1016/B978-0-12-818712-8.00007-0.
- [148] M. Wang and H. Hu, “Fusion welding of vacuum high pressure die cast aluminum alloy A356 and wrought alloy 6061,” *SAE Int. J. Mater. Manuf.*, vol. 6, no. 2, pp. 299–303, 2013, doi: 10.4271/2013-01-1026.
 - [149] T. Soysal, S. Kou, D. Tat, and T. Pasang, “Macrosegregation in dissimilar-metal fusion welding,” *Acta Mater.*, vol. 110, pp. 149–160, 2016, doi: 10.1016/j.actamat.2016.03.004.
 - [150] N. Z. Khan, A. N. Siddiquee, and Z. A. Khan, *Friction stir welding: Dissimilar aluminum alloys*, First ed. Boca Raton: CRC Press, 2017.
 - [151] G. Çam, V. Javaheri, and A. Heidarzadeh, “Advances in FSW and FSSW of dissimilar Al-alloy plates,” *J. Adhes. Sci. Technol.*, vol. 0, no. 0, pp. 1–33, 2022, doi: 10.1080/01694243.2022.2028073.
 - [152] W. M. Thomas, “Friction - friend or foe ? Metal deposition by friction and resistance techniques,” no. June, 2015.
 - [153] J. Zhao, C. S. Wu, and L. Shi, “Effect of ultrasonic field on microstructure evolution in friction stir welding of dissimilar Al/Mg alloys,” *J. Mater. Res. Technol.*, vol. 17, pp. 1–21, 2022, doi: 10.1016/j.jmrt.2021.12.133.
 - [154] J. Mohammadi *et al.*, “Friction stir welding joint of dissimilar materials between AZ31B magnesium and 6061 aluminum alloys: Microstructure studies and mechanical characterizations,” *Mater. Charact.*, vol. 101, pp. 189–207, 2015, doi: 10.1016/j.matchar.2015.01.008.
 - [155] M. De Giorgi, A. Scialpi, F. W. Panella, and L. A. C. De Filippis, “Effect of shoulder geometry on residual stress and fatigue properties of AA6082 FSW joints,” *J. Mech. Sci. Technol.*, vol. 23, no. 1, pp. 26–35, 2009, doi: 10.1007/s12206-008-1006-4.
 - [156] L. Nie, Y. X. Wu, and H. Gong, “Prediction of temperature and residual stress distributions in friction stir welding of aluminum alloy,” *Int. J. Adv. Manuf. Technol.*, vol. 106, no. 7–8, pp. 3301–3310, 2020, doi: 10.1007/s00170-019-04826-4.
 - [157] H. J. Aval, “Microstructure and residual stress distributions in friction stir welding of dissimilar aluminium alloys,” *Mater. Des.*, vol. 87, pp. 405–413, 2015, doi: 10.1016/j.matdes.2015.08.050.
 - [158] S. L. Campanelli, G. Casalino, C. Casavola, and V. Moramarco, “Analysis and comparison of friction stir welding and laser assisted friction stir welding of aluminum alloy,” *Materials (Basel)*, vol. 6, no. 12, pp. 5923–5941, 2013, doi: 10.3390/ma6125923.
 - [159] L. Liu, *Welding and joining of magnesium alloys to aluminum alloys*, no. 2008. Woodhead Publishing Limited, 2010. doi: 10.1533/9780857090423.1.38.
 - [160] R. W. McCune, A. Murphy, M. Price, and J. Butterfield, “The influence of friction stir welding process idealization on residual stress and distortion predictions for future airframe assembly simulations,” *J. Manuf. Sci. Eng.*, vol. 134, no. 3, pp. 1–9, 2012, doi: 10.1115/1.4006554.

- [161] K. P. Mehta and R. Patel, "On fsf keyhole removal to improve volume defect using pin less tool," *Key Eng. Mater.*, vol. 821 KEM, pp. 215–221, 2019, doi: 10.4028/www.scientific.net/KEM.821.215.
- [162] M. R. James, "Relaxation of Residual Stresses - an Overview.," *Adv Sur Treat, Technol - Appl - Eff.*, vol. 4, pp. 349–365, 1987, doi: 10.1016/b978-0-08-034062-3.50026-4.
- [163] J. I. Goldstein *et al.*, *Scanning Electron Microscopy and X-Ray Microanalysis*, THIRD EDIT. New York: Plenum Publishers, 2003. doi: 10.2307/3225926.
- [164] P. Dong, D. Sun, and H. Li, "Natural aging behaviour of friction stir welded 6005A-T6 aluminium alloy," *Mater. Sci. Eng. A*, vol. 576, pp. 29–35, 2013, doi: 10.1016/j.msea.2013.03.077.
- [165] D. Li, X. Yang, L. Cui, F. He, and H. Shen, "Effect of welding parameters on microstructure and mechanical properties of AA6061-T6 butt welded joints by stationary shoulder friction stir welding," *Mater. Des.*, vol. 64, pp. 251–260, 2014, doi: 10.1016/j.matdes.2014.07.046.
- [166] B. R. Singh, *A Hand Book on Friction Stir Welding*, no. August. 2012. doi: 10.13140/RG.2.1.5088.6244.
- [167] Perkin Elmer and K. Neubauer, "Analysis of Aluminum Alloys with the Avio 220 Max ICP-OES Following London Metal Exchange Guidelines." 2021.
- [168] D. G. Andrade, C. Leitão, N. Dialami, M. Chiumenti, and D. M. Rodrigues, "Modelling torque and temperature in friction stir welding of aluminium alloys," *Int. J. Mech. Sci.*, vol. 182, no. March, 2020, doi: 10.1016/j.jimecs.2020.105725.
- [169] S. Nishikawa and S. Kikuchi, "The Diffraction of Cathode Rays by Calcite," *Proc. Imp. Acad.*, vol. 4, no. 8, pp. 475–477, 1928, doi: 10.2183/pjab1912.4.475.
- [170] P. Zhang *et al.*, "Plastic deformation behavior of the friction stir welded AA2024 aluminum alloy," *Int. J. Adv. Manuf. Technol.*, vol. 74, no. 5–8, pp. 673–679, 2014, doi: 10.1007/s00170-014-6031-0.
- [171] T. T. Feng, X. H. Zhang, G. J. Fan, and L. F. Xu, "Effect of the rotational speed of on the surface quality of 6061 Al-alloy welded joint using friction stir welding," *IOP Conf. Ser. Mater. Sci. Eng.*, vol. 213, no. 1, 2017, doi: 10.1088/1757-899X/213/1/012047.
- [172] M. Hadi, M. AL-Khafaji, and A. Subhi, "A Numerical Study of Friction Stir Welding for AA5754 Sheets to Evaluate Temperature Profile and Plastic Strain," *Eng. Technol. J.*, vol. 40, no. 12, pp. 1–12, 2022, doi: 10.30684/etj.2022.134304.1237.
- [173] M. M. Attallah and H. G. Salem, "Friction stir welding parameters: A tool for controlling abnormal grain growth during subsequent heat treatment," *Mater. Sci. Eng. A*, vol. 391, no. 1–2, pp. 51–59, 2005, doi: 10.1016/j.msea.2004.08.059.
- [174] H. G. Salem, A. P. Reynolds, and J. S. Lyons, "Structural Evolution and Superplastic Formability of Friction Stir Welded AA 2095 Sheets," *J. Mater. Eng. Perform.*, vol. 13, no. 1, pp. 24–31, 2004, doi: 10.1361/10599490417551.

- [175] J. L. Todolí and J. M. Mermet, "Sample introduction systems for the analysis of liquid microsamples by ICP-AES and ICP-MS," *Spectrochim. Acta - Part B At. Spectrosc.*, vol. 61, no. 3, pp. 239–283, 2006, doi: 10.1016/j.sab.2005.12.010.
- [176] C. Zhang, J. D. Robson, S. J. Haigh, and P. B. Prangnell, "Interfacial Segregation of Alloying Elements During Dissimilar Ultrasonic Welding of AA6111 Aluminum and Ti6Al4V Titanium," *Metall. Mater. Trans. A Phys. Metall. Mater. Sci.*, vol. 50, no. 11, pp. 5143–5152, 2019, doi: 10.1007/s11661-019-05395-7.
- [177] G. G. Yeshitla and B. Singh, "Friction Stir Welding and its Applications: An Overview," *AIP Conf. Proc.*, vol. 2681, no. 11, pp. 682–705, 2022, doi: 10.1063/5.0116234.
- [178] A. Arora, M. Mehta, A. De, and T. Debroy, "Load bearing capacity of tool pin during friction stir welding," *Int. J. Adv. Manuf. Technol.*, vol. 61, no. 9–12, pp. 911–920, 2012, doi: 10.1007/s00170-011-3759-7.
- [179] Z. FENG, *Processes and mechanisms of welding residual stress and distortion*. Cambridge England: Woodhead, 2005.
- [180] R. H. Leggatt, "Residual stresses in welded structures," *Int. J. Press. Vessel. Pip.*, vol. 85, pp. 144–151, 2008, doi: 10.1016/j.ijvp.2007.10.004.
- [181] N. Jayaraman, P. Prev  y, and M. Mahoney, "Fatigue life improvement of an aluminum alloy FSW with low plasticity burnishing," in *TMS Annual Meeting*, 2003, no. 513, pp. 259–269.
- [182] W. Woo *et al.*, "In situ neutron diffraction measurements of temperature and stresses during friction stir welding of 6061-T6 aluminium alloy," *Sci. Technol. Weld. Join.*, vol. 12, no. 4, pp. 298–303, 2007, doi: 10.1179/174329307X197548.
- [183] ASTM E8, "ASTM E8/E8M standard test methods for tension testing of metallic materials 1," *Annu. B. ASTM Stand.* 4, pp. 1–27, 2010, doi: 10.1520/E0008.
- [184] Y. Pauleau, *Materials Surface Processing by Directed Energy Techniques*. Elsevier Science, 2006.
- [185] S. Kianfar, "The technological foundations of residual stress evolution in manufacturing of next-generation lightweight automotive powertrain components," 2022.
- [186] P. Hofer, E. Kaschnitz, and P. Schumacher, "Distortion and Residual Stress in High-Pressure Die Castings: Simulation and Measurements," *Jom*, vol. 66, no. 9, pp. 1638–1646, 2014, doi: 10.1007/s11837-014-1118-5.
- [187] S. Itoh, S. Okano, M. Mochizuki, and H. Murakawa, "Influence of initial residual stress by the rolling process on welding distortion of stiffened plates," *Proc. Int. Offshore Polar Eng. Conf.*, vol. 1, pp. 338–344, 2009.
- [188] Q. Wu, J. Wu, Y. Du Zhang, H. J. Gao, and D. Hui, "Analysis and homogenization of residual stress in aerospace ring rolling process of 2219 aluminum alloy using thermal stress relief method," *Int. J. Mech. Sci.*, vol. 157–158, no. April, pp. 111–118, 2019, doi: 10.1016/j.ijmecsci.2019.04.040.

- [189] S. Kianfar, E. Aghaie, J. Stroh, D. Sediako, and J. Tjong, “Residual stress, microstructure, and mechanical properties analysis of HPDC aluminum engine block with cast-in iron liners,” *Mater. Today Commun.*, vol. 26, no. October 2020, p. 101814, 2021, doi: 10.1016/j.mtcomm.2020.101814.
- [190] K. Fraser, “Robust and efficient meshfree solid thermo-mechanics simulation of friction stir welding,” Université du Québec à Chicoutimi, UQAC, 2017.
- [191] G. G. Stubblefield, K. Fraser, B. J. Phillips, J. B. Jordon, and P. G. Allison, “A meshfree computational framework for the numerical simulation of the solid-state additive manufacturing process, additive friction stir-deposition (AFS-D),” *Mater. Des.*, vol. 202, 2021, doi: 10.1016/j.matdes.2021.109514.
- [192] G. G. Stubblefield *et al.*, “Elucidating the influence of temperature and strain rate on the mechanics of AFS-D through a combined experimental and computational approach,” *J. Mater. Process. Technol.*, vol. 305, no. March, p. 117593, 2022, doi: 10.1016/j.jmatprotec.2022.117593.
- [193] M. I. Costa, C. Leitão, and D. M. Rodrigues, “Parametric study of friction stir welding induced distortion in thin aluminium alloy plates: A coupled numerical and experimental analysis,” *Thin-Walled Struct.*, vol. 134, no. June 2018, pp. 268–276, 2019, doi: 10.1016/j.tws.2018.10.027.
- [194] A. Arora, T. Debroy, and H. K. D. H. Bhadeshia, “Back-of-the-envelope calculations in friction stir welding - Velocities, peak temperature, torque, and hardness,” *Acta Mater.*, vol. 59, no. 5, pp. 2020–2028, 2011, doi: 10.1016/j.actamat.2010.12.001.
- [195] B. Medina and R. Fernández, “Material Behavior around the FSW/FSP Tool Described by Molecular Dynamics,” *J. Manuf. Mater. Process.*, vol. 7, no. 1, 2023, doi: 10.3390/jmmp7010013.
- [196] T. Long, W. Tang, and A. P. Reynolds, “Process response parameter relationships in aluminium alloy friction stir welds,” *Sci. Technol. Weld. Join.*, vol. 12, no. 4, pp. 311–317, 2007, doi: 10.1179/174329307X197566.
- [197] R. . Gingold and J. . Monaghan, “Smoothed particle hydrodynamics: theory and application to non-spherical stars”.
- [198] L. . Lucy, “A numerical approach to the testing of the fission hypothesis,” *Astron. J.*, vol. 82, no. 12, pp. 1013–1024, 1977.
- [199] K. Fraser, “Adaptive Smoothed Particle Hydrodynamics Neighbor Search Algorithm for Large Plastic Deformation Computational Solid Mechanics,” in *Proceedings of the 13th International LS-DYNA Users Conference*, 2014, pp. 1–15.
- [200] J. Yan, M. A. Sutton, and A. P. Reynolds, “Process-structure-property relationships for nugget and heat affected zone regions of AA2524-T351 friction stir welds,” *Sci. Technol. Weld. Join.*, vol. 10, no. 6, pp. 725–736, 2005, doi: 10.1179/174329305X68778.
- [201] C. Hamilton, M. Kopyściański, O. Senkov, and S. Dymek, “A coupled thermal/material flow model of friction stir welding applied to Sc-modified aluminum alloys,” *Metall. Mater. Trans. A Phys. Metall. Mater. Sci.*, vol. 44, no. 4, pp. 1730–1740, 2013, doi:

10.1007/s11661-012-1512-y.

- [202] R. Yadav, A. Kumar, G. P. Chaudhari, and A. G. Paradkar, "Mechanical and stress corrosion cracking behavior of welded 5059H116 alloy," *Corros. Sci.*, vol. 206, no. January, p. 110528, 2022, doi: 10.1016/j.corsci.2022.110528.
- [203] N. . Lopatin, "Temperature and geological time as a factor in coalification," *Proc. USSR Acad. Sci.*, vol. 2, pp. 95–106, 1971, doi: 10.1144/1354-079304-636.
- [204] S. Lomolino, R. Tovo, and J. Dos Santos, "On the fatigue behaviour and design curves of friction stir butt-welded Al alloys," *Int. J. Fatigue*, vol. 27, no. 3, pp. 305–316, 2005, doi: 10.1016/j.ijfatigue.2004.06.013.
- [205] H. G. Salem, "Friction stir weld evolution of dynamically recrystallized AA 2095 weldments," *Scr. Mater.*, vol. 49, no. 11, pp. 1103–1110, 2003, doi: 10.1016/j.scriptamat.2003.08.010.
- [206] M. Guerra, C. Schmidt, J. C. McClure, L. E. Murr, and A. C. Nunes, "Flow patterns during friction stir welding," *Mater. Charact.*, vol. 49, no. 2, pp. 95–101, 2002, doi: 10.1016/S1044-5803(02)00362-5.
- [207] H. Agiwal *et al.*, "Material flow visualization during friction stir welding using high-speed X-ray imaging," *Manuf. Lett.*, vol. 34, pp. 62–66, 2022, doi: 10.1016/j.mfglet.2022.08.016.
- [208] D. Bajaj *et al.*, "Flow, process forces and strains during Friction Stir Welding: A comprehensive First principle approach," *Proc. Inst. Mech. Eng. Part B J. Eng. Manuf.*, vol. 235, no. 5, pp. 912–924, 2021, doi: 10.1177/0954405420970087.
- [209] C. Casavola, A. Cazzato, and V. Moramarco, "Residual Stress in Friction Stir Welding and Laser-Assisted Friction Stir Welding by Numerical Simulation and Experiments," *Residual Stress Anal. Welded Joints by Means Numer. Simul. Exp.*, 2018, doi: 10.5772/intechopen.72271.
- [210] T. Wang, T. Liu, T. Roosendaal, and P. Upadhyay, "Reinforcing the exit hole from friction stir welding and processing," *Materialia*, vol. 26, no. October, p. 101611, 2022, doi: 10.1016/j.mtla.2022.101611.
- [211] T. U. Seidel and A. P. Reynolds, "Visualization of the material flow in AA2195 friction-stir welds using a marker insert technique," *Metall. Mater. Trans. A Phys. Metall. Mater. Sci.*, vol. 32, no. 11, pp. 2879–2884, 2001, doi: 10.1007/s11661-001-1038-1.
- [212] H. N. B. Schmidt, T. L. Dickerson, and J. H. Hattel, "Material flow in butt friction stir welds in AA2024-T3," *Acta Mater.*, vol. 54, no. 4, pp. 1199–1209, 2006, doi: 10.1016/j.actamat.2005.10.052.
- [213] K. N. Krishnan, "On the formation of onion rings in friction stir welds," *Mater. Sci. Eng. A*, vol. 327, no. 2, pp. 246–251, 2002, doi: 10.1016/S0921-5093(01)01474-5.
- [214] V. Randle and O. Engler, *Introduction to Texture Analysis Macrotexture, Microtexture and Orientation Mapping*, vol. 38, no. 4. New York: CRC Press, 2005. doi: 10.1016/j.dss.2003.08.004.

- [215] L. Zhang *et al.*, “Texture, microstructure and mechanical properties of 6111 aluminum alloy subject to rolling deformation,” *Mater. Res.*, vol. 20, no. 5, pp. 1360–1368, 2017, doi: 10.1590/1980-5373-MR-2017-0549.
- [216] S. Emami, T. Saeid, and A. Abdollah-zadeh, “Effect of friction stir welding parameters on the microstructure and microtexture evolution of SAF 2205 stainless steel,” *J. Alloys Compd.*, vol. 810, p. 151797, 2019, doi: 10.1016/j.jallcom.2019.151797.
- [217] H. Lyu, Y. Zhang, Y. Bao, and J. Zhang, “The Effect of Initial Texture on the Plastic Deformation of Gradient Aluminum,” *Materials (Basel)*, vol. 16, no. 7, 2023, doi: 10.3390/ma16072603.
- [218] W. Woo, H. Choo, D. W. Brown, P. K. Liaw, and Z. Feng, “Texture variation and its influence on the tensile behavior of a friction-stir processed magnesium alloy,” *Scr. Mater.*, vol. 54, no. 11, pp. 1859–1864, 2006, doi: 10.1016/j.scriptamat.2006.02.019.
- [219] S. Bhardwaj, S. K. Aas, and R. M. C. Ratnayake, “Experimental and numerical investigation of residual stresses in proximity girth welds,” *Int. J. Adv. Manuf. Technol.*, vol. 126, no. 3–4, pp. 1247–1259, 2023, doi: 10.1007/s00170-023-11162-1.
- [220] S. Sun, J. Wang, B. Gao, G. Cao, and S. Zhang, “Influence of Friction Stir Weld Seam Distribution on Spun Formed Workpieces,” *Int. J. Precis. Eng. Manuf.*, vol. 25, no. 6, pp. 1183–1194, 2024, doi: 10.1007/s12541-024-00994-w.
- [221] J. P. Martin, C. Stanhope, and S. Gascoyne, “Novel techniques for corner joints using friction stir welding,” *TMS Annu. Meet.*, pp. 179–186, 2011, doi: 10.1002/9781118062302.ch22.
- [222] J. S. Agapiou and B. E. Carlson, “Friction stir welding for assembly of copper squirrel cage rotors for electric motors,” *Procedia Manuf.*, vol. 48, no. 2019, pp. 1143–1154, 2020, doi: 10.1016/j.promfg.2020.05.156.
- [223] C. Dalle Donne, G. Biallas, T. Ghidini, and G. Raimbeaux, “Effect of Weld Imperfections and Residual Stresses on the Fatigue Crack Propagation in Friction Stir Welded Joints,” in *Second International Conference on Friction Stir Welding*, 2000, vol. 26–28. doi: 10.1520/JAI12036.
- [224] C. M. Chen and R. Kovacevic, “Parametric finite element analysis of stress evolution during friction stir welding,” *Proc. Inst. Mech. Eng. Part B J. Eng. Manuf.*, vol. 220, no. 8, pp. 1359–1371, 2006, doi: 10.1243/09544054JEM324.
- [225] C. A. Charitidis, D. A. Dragatogiannis, E. P. Koumoulos, and I. A. Kartsonakis, “Residual stress and deformation mechanism of friction stir welded aluminum alloys by nanoindentation,” *Mater. Sci. Eng. A*, vol. 540, pp. 226–234, 2012, doi: 10.1016/j.msea.2012.01.129.
- [226] T. W. Clyne and J. E. Campbell, *Testing of the Plastic Deformation of Metals*. 2021. doi: 10.1017/9781108943369.
- [227] J. Kozakevich, “UBC HPPM internal report,” 2024.
- [228] X. Xu, H. Yu, and Z. Lin, “Study of residual stress variation with depth of friction stir

- welded aluminium plates with different thicknesses,” *Sci. Technol. Weld. Join.*, vol. 25, no. 4, pp. 297–302, 2020, doi: 10.1080/13621718.2019.1693722.
- [229] W. He, J. Liu, W. Hu, G. Wang, and W. Chen, “Controlling residual stress and distortion of friction stir welding joint by external stationary shoulder,” *High Temp. Mater. Process.*, vol. 38, no. 2019, pp. 662–671, 2019, doi: 10.1515/htmp-2019-0005.
- [230] P. Shahi, M. Barmouz, and P. Asadi, *Force and torque in friction stir welding*. Woodhead Publishing Limited, 2014. doi: 10.1533/9780857094551.459.
- [231] B. Meyghani and C. Wu, “Progress in Thermomechanical Analysis of Friction Stir Welding,” *Chinese J. Mech. Eng. (English Ed.)*, vol. 33, no. 1, 2020, doi: 10.1186/s10033-020-0434-7.
- [232] M. E. Sigl, A. Bachmann, T. Mair, and M. F. Zaeh, “Torque-based temperature control in friction stir welding by using a digital twin,” *Metals (Basel)*, vol. 10, no. 7, pp. 1–18, 2020, doi: 10.3390/met10070914.
- [233] C. K. Jin, “Microstructures and Mechanical Properties of A356 Alloy and A6061 Alloy through Rheo, Upsetting, Thixo, Forming Process, and T6 Heat Treatment,” *Metals (Basel)*, vol. 12, no. 12, 2022, doi: 10.3390/met12122051.
- [234] F. C. B. C. Hernandez, J. H. Sokolowski, and J. J. De Cruz Rivera, “Micro-Raman analysis of the Si particles present in Al-Si hypereutectic alloys in liquid and semi-solid states,” *Adv. Eng. Mater.*, vol. 9, no. 1–2, pp. 46–51, 2007, doi: 10.1002/adem.200600173.
- [235] W. Tang, X. Guo, J. C. McClure, L. E. Murr, and A. Nunes, “Heat input and temperature distribution in friction stir welding,” *J. Mater. Process. Manuf. Sci.*, vol. 7, no. 2, pp. 163–172, 1998, doi: 10.1106/55TF-PF2G-JBH2-1Q2B.
- [236] P. C. Sinclair, W. R. Longhurst, C. D. Cox, D. H. Lammlein, A. M. Strauss, and G. E. Cook, “Heated friction stir welding: An experimental and theoretical investigation into how preheating influences process forces,” *Mater. Manuf. Process.*, vol. 25, no. 11, pp. 1283–1291, 2010, doi: 10.1080/10426914.2010.496122.
- [237] D. Radaj, *Heat Effects of Welding*. 1992. doi: 10.1007/978-3-642-48640-1.
- [238] M. T. Milan, W. W. Bose Filho, and J. R. Tarpani, “Fatigue crack growth behavior of friction stir welded 2024-T3 aluminum alloy tested under accelerated salt fog exposure,” *Mater. Perform. Charact.*, vol. 3, no. 3, pp. 232–251, 2014, doi: 10.1520/MPC20130036.