

(In)efficacy of Architected Auxetic Materials for Impact Mitigation

TIL GÄRTNER ^{a,b,*}, RICHARD DEKKER ^b, DENNIS VAN VEEN^b, SANNE J. VAN DEN BOOM ^d, AND LUCAS AMARAL^{b,†}

^aFaculty of Civil Engineering and Geosciences, Delft University of Technology

^bUnit Defense, Saftety, and Security,

Netherlands Institute for Applied Scientific Research (TNO)

^cUnit High Tech Industry, Netherlands Institute for Applied Scientific Research (TNO)

^{*}Corresponding Author: t.gartner@tudelft.nl

[†]Corresponding Author: lucas.amaral@tno.nl

January 10, 2025

Abstract

It has been demonstrated that auxetic materials, characterized by a negative Poisson's ratio, offer enhanced resistance to indentation, shear forces, fracture toughness and the absorption of energy. As such, they are reported in literature to be promising options for impact mitigation in military and space contexts. Auxetic materials are rare in nature, and must therefore be designed and manufactured artificially in order to be applied. Densification of auxetic materials in order to absorb impact energy in a limited area has been the focus in the literature to date. However, this results in a concentration of the force paths, which is not desirable for impact mitigation. In this work, the effects of auxetic densification on the stress distribution over the backside of the auxetic material are addressed using both experimental and simulative trials. In this study, the distinction between auxetic and conventional honeycombs in force transmission characteristics is examined. This is achieved through an analysis of experimental data and the utilization of numerical techniques to enhance comprehension of the internal mechanisms of architected materials in response to impact.

1 Introduction

Lightweight impact protection, formerly a concern of aerospace designers due to the typical weight constraints of aircraft, has also become of interest for other applications such as vehicles, ships and infrastructure protection. Lightweight design allows for more fuel efficiency and better mobility of platforms [6], and also for using less material in infrastructure construction, contributing to sustainability goals and energy efficiency. Amongst different innovative solutions for lightweight impact protection, auxetic structures have received significant attention in recent research [1, 17, 7]. Due to the negative Poisson's ratio of auxetic materials, densification occurs when such materials are impacted, drawing material towards the impacted area, such as illustrated in Figure 1.1. Because of this negative Poisson's ratio effect, auxetics are often considered as promising candidates to include in lightweight impact protection [1, 7].

Given the absence of natural materials exhibiting negative Poisson's ratio, auxetic structures need to be architected from artificial materials to exhibit such negative Poisson's ratio for practical use. Whilst creating auxetic foams is possible [4], in impact protection literature the negative Poisson's ratio is however mostly achieved by designing auxetic lattice structures [1], such as exemplified by the unit cell in Figure 1.2b. These unit cells can be repeated to create a larger lattice structure with properties resembling the unit cell. Figure 1.2a shows the conventional honeycomb (CH), a non-auxetic unit cell, while Figure 1.2b shows the auxetic re-entrant honeycomb (ARH), an auxetic unit cell. The use of such auxetic lattices for impact protection reaches back decades [18], but widespread experimentation with such designs has recently become popular due to advances in manufacturing techniques such as 3D

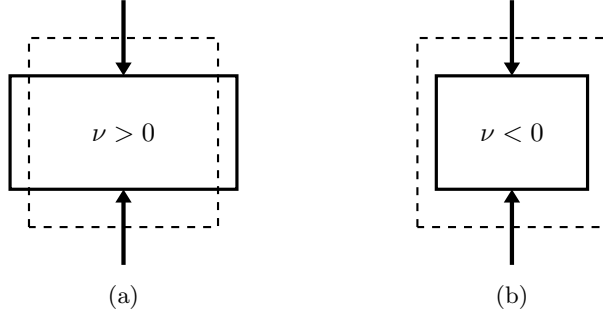


Figure 1.1: (a) Positive Poisson's ratio materials ($\nu > 0$) and (b) negative Poisson's ratio or auxetic materials ($\nu < 0$) under compression.

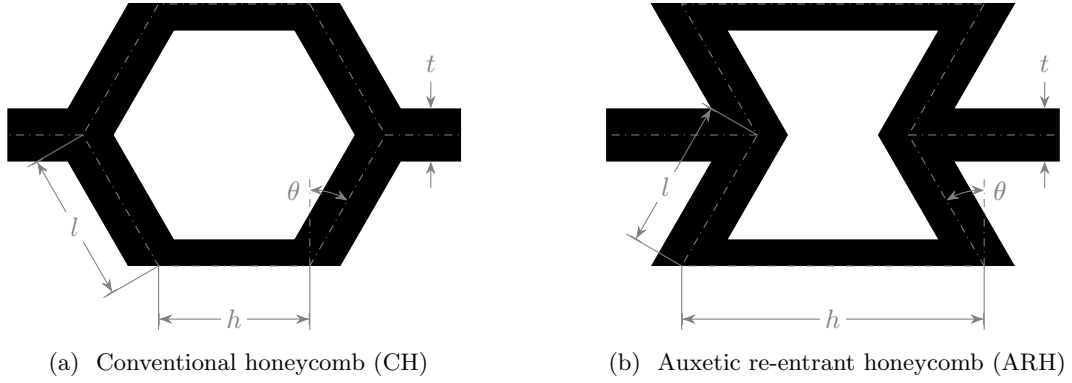


Figure 1.2: Comparison of (a) conventional and (b) auxetic re-entrant honeycomb lattice structures.

printing. Whilst experimental campaigns require significant efforts in time and money, finite element (FE) modelling approaches provide a fast way to supplement the experimental findings and have been a staple in designing and improving auxetic materials in the last decades [7]. Within the FE modelling, one can speed up the computation even further by abstracting the present structure into its structural elements instead of viewing the structure as three-dimensional solids (e.g. [24, 13, 8]).

Because the beneficial properties of auxetic materials are promising in the search for lightweight impact protection, a plethora of studies on auxetic lattices used as blast and ballistic protection has been published in the past years, many of them focusing on the ARH as a simple auxetic solution [1]. Although such studies indicate the ARH almost unanimously as a better choice for impact and blast protection than conventional lattices [1, 2, 15], the published research fails to articulate why or how designers should incorporate such auxetic lattices in real applications, and what design parameters the designer should focus on to enable impact mitigation.

Consider, as an example, the illustration in Figure 1.3. Materials with a positive Poisson's ratio will spread the load onto a wider area, as illustrated in Figure 1.3b. Negative Poisson's ratio materials (Figure 1.3c) on the other hand, concentrate the load onto a smaller area. This effectively means that the distributed force results in less pressure being exerted on the protected structure. This anecdotal example aims to illustrate in simple terms that not in all cases auxetic protection systems will over-perform non-auxetic conventional protection systems, contrary to what is concluded in [1].

When compared to the non-auxetic CH, the ARH has been shown to fold more easily when impacted, leading to earlier densification in the progress [15, 14]. This earlier densification is related to the inwards folding of the ARH structures, generating the auxetic effect. Once the material is densified, it acts as a conventional solid material and thus generates a substantially higher stress level, contributing to energy dissipation. Available research focuses its conclusions on the higher energy dissipated by auxetic lattices [15, 14], to claim the better performance in impact scenarios, but little is reported about other metrics to evaluate performance under impact and where this dissipated energy goes to: The effects of high peak reaction forces generated by the impacted ARH due to its densification, being transferred to the underlying structure, is currently not addressed in the available literature. The temporal and

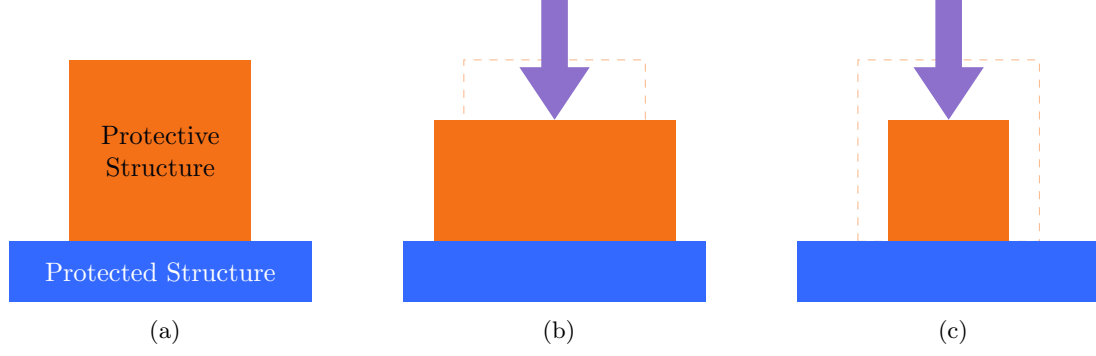


Figure 1.3: Illustration of an example where auxetic lattices might be detrimental to the structure to be protected. (a) Unloaded structure; (b) structure with positive Poisson's ratio under load; (c) structure with a negative Poisson's ratio under load. Contact area is larger for structure with positive Poisson's ratio, transferring smaller stresses to underlying structure. Dashed lines represent the shape of the undeformed structure.

spatial distribution of the impact energy and the subsequent effects on the protection capabilities of different lattice structures have not been addressed in sufficient detail thus far. However, some studies have shown in the past that load spreading is essential for effective impact protection [11]. The transfer of a substantial quantity of energy over a brief time span to a restricted area, as a consequence of the densification of the distinct lattice structures, may prove detrimental to the efficacy of the protective measures in question. More specifically, Gupta and Ding [11] mention that absorbing energy is not enough, but that momentum has to be diverted from the impactor and distributed laterally. The structure underlying the protective layer is usually not designed to withstand high, localized loads and thus is dependent on the protective layer to spread out the load over both time and space. The only instance found in literature where the auxetic lattices are actually shown to transfer less force to the underlying structure than the CH is described in Bohara et al. [2]. The authors show that, under close-in blast load, the ARH deflects part of the blast overpressure load. The increased densification of the ARH, by drawing material to the blast-impacted zone, makes the core stiffer under the close-in blast area and more compliant on the edges of the panel, contributing to the deflection of the blast pressure. [2]

In addition to the load transfer of single lattice structures, the design parameters for auxetic lattice structures, including the design choices in relation to impact loads, had not been discussed in-depth until the recent publication of Gärtner et al. [8]. In this publication, it is shown that, for the effect of the auxeticity to have the greatest energy absorption, the impact should be localized and not over the complete length of the architected material. Localized impact results in the drawing of unloaded material from the sides toward the impact zone, thereby facilitating a wider degree of densification. Furthermore, the impact energy can be transported further laterally. However, only geometric non-linear elastic models without contact were used in that study, and no experimental validation was presented. [8]

It is currently unclear whether auxetic lattices can be shown to outperform conventional lattices in impact protection applications in the real world, particularly in cases where energy dissipation is not the sole measure of performance. This contribution thus proposes an integrated approach to the design and evaluation of auxetic lattice structures for protection, which goes beyond mere consideration of their energy dissipation capabilities. It is important to note that the objective of this study is not to disprove the use of auxetics. Instead, it is to subject the metrics of performance applied to auxetics in impact protection, as well as the underlying physical mechanisms, to rigorous scrutiny. The aim is to provide a better foundation for the application of auxetic or conventional lattices as protective structures.

1.1 Objectives

The aim of this study is twofold:

Firstly, this study aims to use different metrics to evaluate and compare the performance under impact of both auxetic and conventional lattice structures. The use of different metrics, such as load transferred to the underlying structure and the distribution of the pressure at the back-face of the

protective structure can give new insights in the mechanisms of impact protection of different lattices, enabling the guided design of lattice structures for impact mitigation in relation to different loading scenarios.

Secondly, in order to produce guidelines for load localization for impact protection design, the present study aims to test the hypothesis of [8], which states that in order for auxeticity to have more effect and a better impact protection, it is crucial, that the impact is localized and does not cover the complete auxetic lattice surface area.

To complete the overview of the design and analysis process of auxetic lattices, the present study combines experiments and non-linear numerical models. Furthermore, this study offers an insight into the numerical assessment methods available for architected lattices used for impact protection, by showcasing investigations possible with both detailed, continuum-based and efficient, beam-based numerical models. The continuum-based numerical model is employed to gain a deeper insight into the details of the inner deformation and load transmission processes of the experimental campaign. To supplement these investigations, the beam-based numerical model is chosen due to its efficiency to enable fast explorations into different scenarios.

1.2 Organization

The lattice structures analysed are presented in Section 2. The different physical and numerical settings employed to assess the performance of different lattice architectures in a protection lay-up are explained in Section 3. Subsequently, the results of the different analyses will be laid out in Section 4, and the advantages and limitations of each analysis will be discussed. A comparison between the different methods will be done in Section 5, as well as a discussion on the efficacy of auxeticity in protective systems and the metrics useful in entertaining this assessment and suggestions for further research.

2 Investigated Architectures

This study investigates architected materials, as shown in Figure 1.2, where Figure 1.2b shows the auxetic re-entrant honeycomb (ARH), an auxetic lattice, while Figure 1.2a shows a conventional honeycomb (CH), a non-auxetic lattice. The ARH has been chosen as the baseline auxetic lattice for comparison. This choice is based on its widespread use in literature (e.g., [1, 2, 15, 14]), and the dimensions of the baseline ARH are described in Table 2.1. In addition, the impact protection performance of the ARH is compared to the same structure rotated by 90° , in the following called auxetic re-entrant honeycomb (90° rotated) (ARH90). In order to shed light on the effects of auxeticity in comparable circumstances, the non-auxetic unit cells conventional honeycomb (W-configuration) (CHW) and conventional honeycomb (L-configuration) (CHL) are investigated as well. The dimensions of the struts of the ARH90, CHW, and CHL samples were selected to guarantee that a set of such samples with the similar mass (SM) as the ARH is obtained. Additionally, another set of unit cells with the same initial elastic stiffness (SS) as the ARH in the impact direction is designed and evaluated throughout the study. The unit cells of each investigated lattice are illustrated in Figure 2.1, while the unit cell masses, dimensions and initial elastic stiffnesses are described in Table 2.1. The initial elastic stiffness of the samples in Figure 2.1 was estimated using linear elastic, small deformation FE analyses considering an elastic modulus of 210 GPa and a Poisson's ratio of 0.265, corresponding to the steel grade used to manufacture the samples. Linear elastic FE analyses were conducted instead of analytical models (such as the one from Gibson et al. [10])

Table 2.1: Dimensions, mass and initial elastic stiffness along impact direction of the lattice geometries unit cells considered in the present study (see Figure 1.2 for the definitions of the dimensions).

	<i>Unit cell</i>	<i>l</i> (mm)	<i>h</i> (mm)	θ ($^\circ$)	<i>t</i> (mm)	mass (g)	stiffness (GPa)
SM	ARH	4.8831	8.30	35	0.750	5.50	2.49
	ARH90	4.8831	8.30	35	0.750	5.50	1.91
	CHW	4.8831	2.65	35	1.100	5.04	5.2
	CHL	4.8831	2.65	35	1.100	5.04	4.71
SS	ARH90	4.8831	8.30	35	0.820	5.98	2.49
	CHW	4.8831	2.65	35	0.854	3.99	2.48
	CHL	4.8831	2.65	35	0.858	4.12	2.49

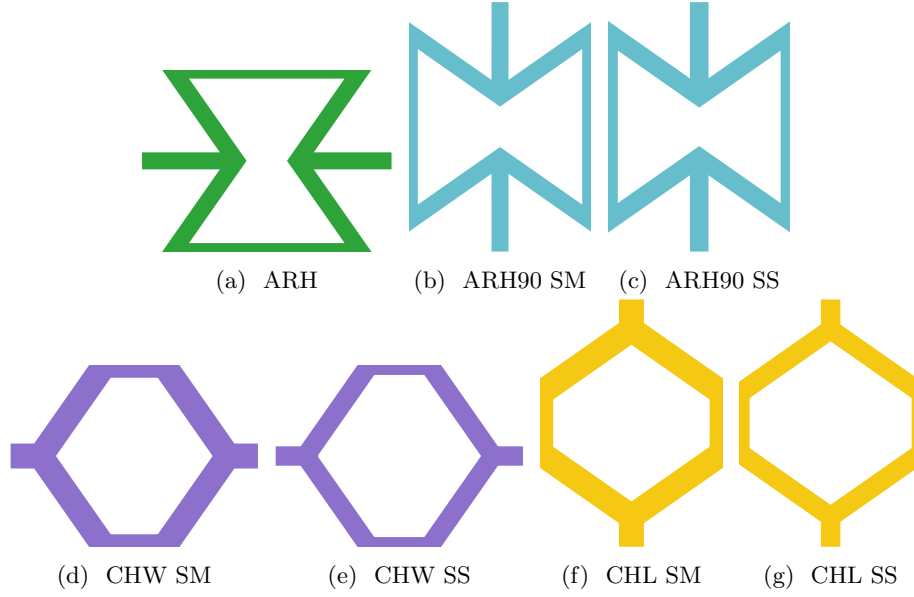


Figure 2.1: unit cells.

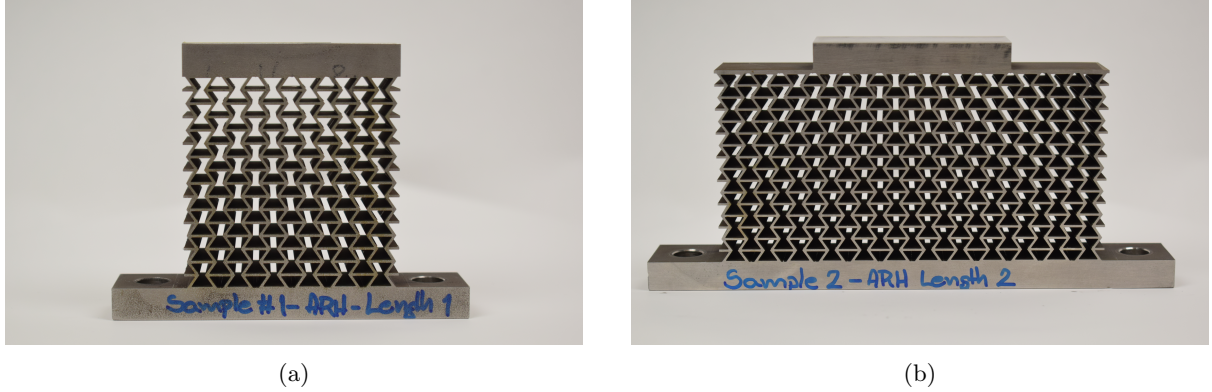


Figure 2.2: Assembly of ARH unit cells into patches to be investigated, (a) *Length 1* and (b) *Length 2*.

to estimate the initial elastic stiffnesses of the samples due to the relatively thick struts of the lattice structures.

2.1 Sample Configuration

The previously described unit cells are assembled into lattice structures with two sizes of the entire patch: One size, in the following denoted as *Length 1*, has approximate measures of $65 \text{ mm} \times 65 \text{ mm}$. A sample twice as wide and denoted as *Length 2*, measures approximately $130 \text{ mm} \times 65 \text{ mm}$. To achieve these measures multiple units cells were stacked both vertically and horizontally, with a base of solid steel, 10 mm high, and a strike face on the upper side being 65 mm wide and likewise 10 mm high. The physical samples produced for the ARH architecture are shown in Figure 2.2 for both lengths investigated. An overview over the number of unit cells in each direction and the resulting mass for the samples is given in Table 2.2. These patches with a depth of 25 mm are investigated for all further experimental and numerical studies unless otherwise noted.

2.2 Test Matrix

In order to assess the effects of the different lattice architectures as well as the effect of localization of an impact, as was suggested by [8], all architectures are tested according to the test matrix shown in

Table 2.2: Measured or estimated mass of the auxetic samples which are shown in Figure 2.2. The samples of *Length 1* were not measured with a digital scale but, instead, the mass was estimated via CAD model assuming a density of 8000 kg m^{-3} .

	<i>Unit cell</i>	<i>Length 1</i>			<i>Length 2</i>		
		n_x	n_y	mass (g)	n_x	n_y	mass (g)
SM	ARH	6	8	733.08	12	8	976.75
	ARH90	8	6	733.15	16	6	977.05
	CHW	6	8	732.61	12	8	970.15
	CHL	8	6	732.75	16	6	971.21
SS	ARH90	8	6	742.35	16	6	1023.05
	CHW	6	8	669.46	12	8	872.96
	CHL	8	6	688.43	16	6	884.48

Table 2.3: Physical & numerical investigation matrix.

	<i>Unit cell</i>	<i>Length 1</i>		<i>Length 2</i>	
		physical	numerical	physical	numerical
SM	ARH	physical	numerical	physical	numerical
	ARH90	only	numerical	physical	numerical
	CHW	only	numerical	physical	numerical
	CHL	only	numerical	physical	numerical
SS	ARH90	only	numerical	physical	numerical
	CHW	only	numerical	physical	numerical
	CHL	only	numerical	physical	numerical

Table 2.3. The effect of impact localization is experimentally carried out only for the ARH as baseline architecture. For the assessment of the other lattice architectures additional FE analyses are conducted.

3 Methods

3.1 Experimental Set-up

3.1.1 Sample Manufacturing

The samples, with geometries as described in the previous section, have been manufactured by electric discharge machining (EDM). EDM was chosen for its tight geometric tolerances, which are required to ensure that the radii in the corners of the structures are sufficiently small to ensure that the physical samples resemble the desired structures. EDM is an inherently precise method, with the accuracy only limited by the size of the electrode, and maintains this accuracy for relatively large material depths.

A good material for lattice samples in impact condition will have a high yield strength and stiffness, in combination with being very ductile. These properties ensure sufficient energy can be dissipated during

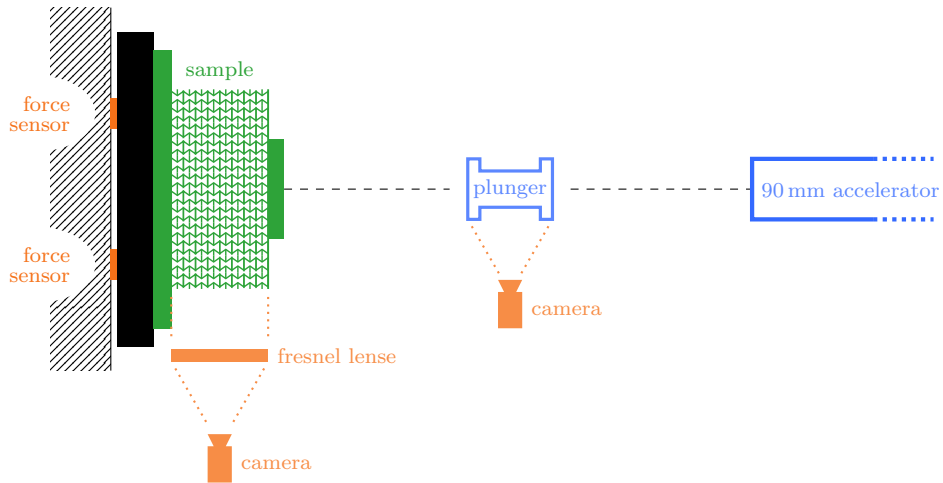


Figure 3.1: Schematic view of the experimental set-up.

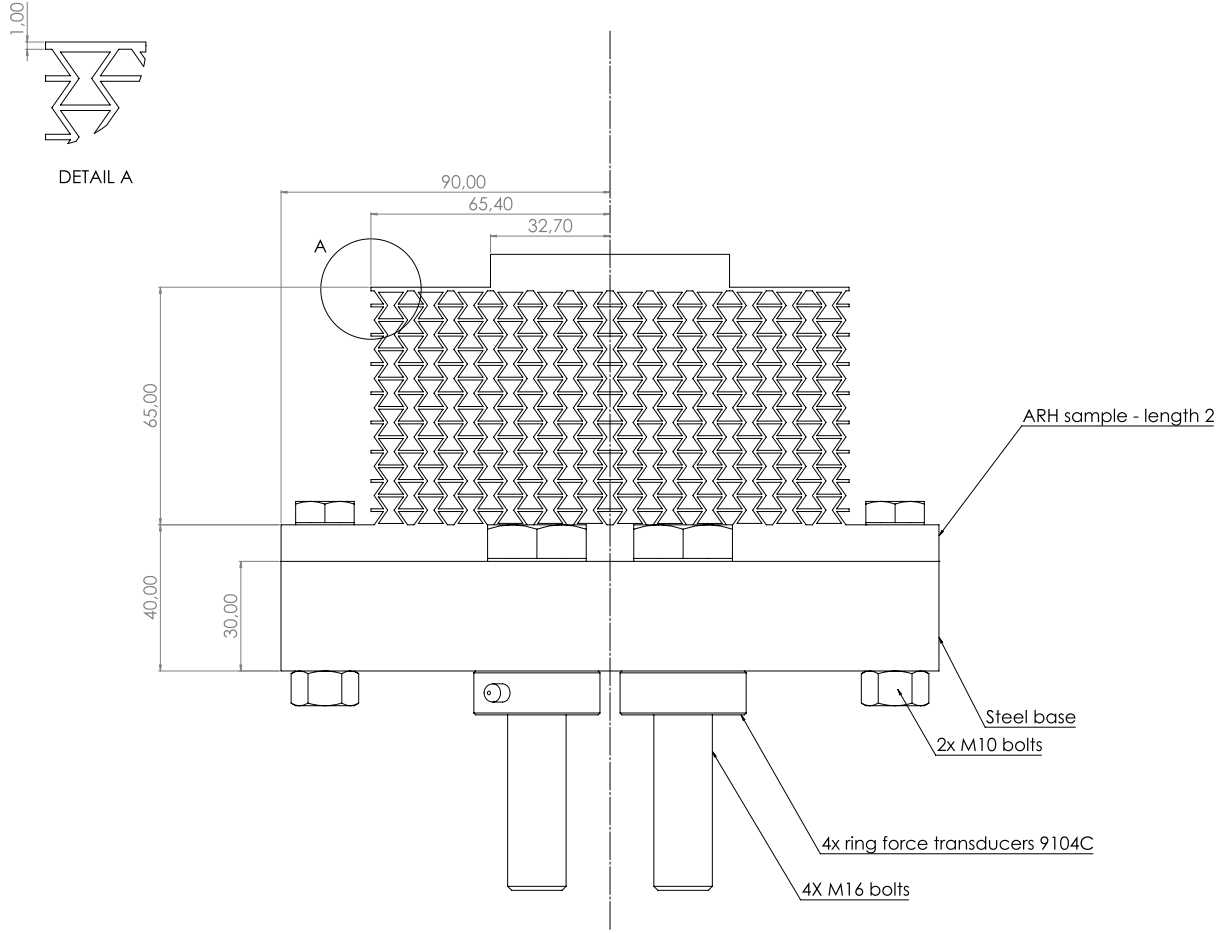


Figure 3.2: Side view of the sample mounting in the experimental set-up with main dimensions and components – dimensions in mm.

the deformation. The only condition that EDM poses on the material is that it conducts electricity, rendering metals a prime choice. After careful evaluation 316 stainless steel (AISI 316L) was chosen for producing the sample set later used in the experimental campaign. This was motivated mainly by the high strain to failure ($> 25\%$), ensuring the structure folding neatly without hinges breaking and losing structural integrity. This high toughness additionally absorbs a significant amount of energy in the process.

3.1.2 Testing Set-up

For the test series, the lattice samples manufactured by EDM were attached to a 30 mm thick steel base using four M10 bolts. A construction drawing of this set-up can be found in Figure 3.2. The base was then attached to a stiff boundary using four M16 bolts and 4 Piezoelectric ring force transducers from the brand Kistler, type 9104C. The Kistler ring force transducers have a load limit of 160 kN and an axial stiffness of $7.5 \text{ kN } \mu\text{m}^{-1}$ per transducer. All lattice samples were impacted by a plunger with a mass of 1.2 kg and made of Aluminium. The experimental set-up is shown in Figure 3.3, while drawings of the plunger are shown in A. All tests are carried out with a nominal impact velocity of 70 m s^{-1} . The experimental procedure was recorded digitally by two high-speed cameras at 25 000 fps, one directed at the lattice to capture its performance, and the other at the plunger mid-flight to assess the projectile orientation and velocity. A schematic overview over the entire set-up can be found in Figure 3.1.



(a) Position of the camera with Fresnel lens. (b) Shooting barrel and camera pointing at the sample.

Figure 3.3: Experimental set-up.

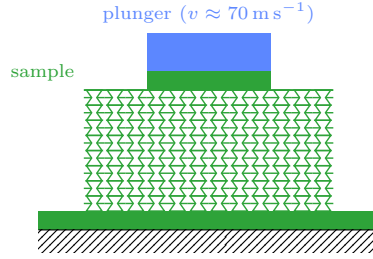


Figure 3.4: Geometry, boundary conditions and initial conditions for the FE analyses.

3.2 Continuum-based FE

Explicit non-linear FE analyses were performed using the commercial software package ABAQUS. An example of a simulated assembly is given in Figure 3.4, which consists out of the lattice specimen and the plunger impacting the specimen. The specimen is fully fixed at its support and the 1.2 kg weighing plunger is given an initial vertical velocity, matching that of the experiments. The plunger was modelled in a simplified manner, as a rectangle of 20 mm height and 65.4 mm width (compare Figure 3.4). Note that, in order to maintain the mass of the plunger the density of this block was adjusted to $36\,703\text{ kg m}^{-3}$. All contact interactions were modelled by hard normal contact and frictionless tangential behaviour. The simulation were performed in 2D, assuming plane strain, where the depth of the specimen and the plunger were set to 25 mm. Four-node quadrilateral elements with reduced integration (type CPE4R), having an average element size of 0.12 mm, were used. This resulted for the ARH sample of *Length 2* in 192 520 elements and 438 206 degrees of freedom (DOFs). A close-up of the mesh showing two unit cells is given in Figure 3.5. As indicated earlier, the lattice is made out of stainless steel and the plunger out of aluminium. Both materials were simulated using a Johnson-Cook plasticity model to account for strain rate effects [12]. In this model, the von Mises yield surface is described by

$$\bar{\sigma} = \left[A + B (\bar{\epsilon}^{\text{pl}})^n \right] \left[1 + C \ln \left(\frac{\dot{\bar{\epsilon}}^{\text{pl}}}{\dot{\bar{\epsilon}}_0} \right) \right], \quad (3.1)$$

with the equivalent plastic strain $\bar{\epsilon}^{\text{pl}}$, the equivalent reference strain rate $\dot{\bar{\epsilon}}_0$ and the numerical parameters A, B, C, n . These properties and parameters used for both materials, following from literature [22, 23], are given in Table 3.1.

Table 3.1: Material properties and parameters for the FE simulation.

Material	E [GPa]	ν [-]	A [MPa]	B [MPa]	C [-]	n [-]	$\dot{\bar{\epsilon}}_0$ [-]	ρ [kg m^{-3}]
Stainless Steel [23]	210	0.265	280	1750	0.1	0.8	0.02	8000
Aluminium [22]	72	0.33	103	350	0.12	0.4	0.001	36 703*

* Note that the density of aluminium is chosen such that the plunger mass is equal to 1.2 kg and is therefore not equal to the real density of aluminium.

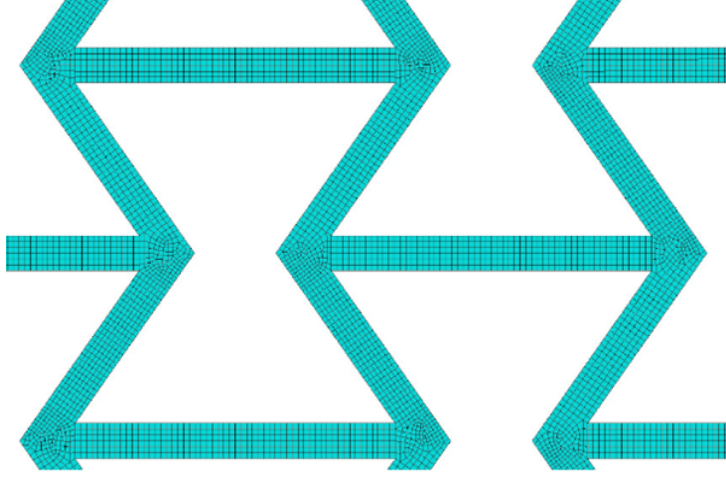


Figure 3.5: Close-up of the mesh for two unit cells.

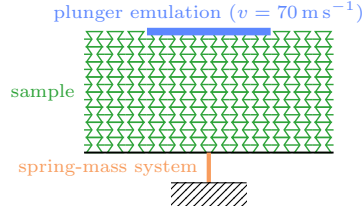


Figure 3.6: Geometry and boundary conditions for the JIVE analyses.

3.3 Beam-based FE

The structures investigated can be viewed as a collection of beams into a lattice. In order to accelerate and simplify the simulations described in Section 3.2, and to allow for a faster processing of different configurations, these lattice structures are also implemented as a collection of Simo-Reissner beams (after [16, 19]). The FE implementation in the JIVE-framework [5] follows [20] with the corrections proposed by [3]. This results for the ARH sample of *Length 2* in 16 824 DOFs, which is a reduction by more than one order of magnitude compared to the continuum-based model in Section 3.2.

The material of the structure is modelled as elasto-plastic beams with kinematic hardening (cf. [9]). For the elastic behaviour a Young's modulus of 210 GPa and a Poisson's ratio of 0.265 are assumed in accordance with the assumptions in the solid model. The beams are modelled as square beams with a side length of 0.75 mm and the shear correction coefficient is set to 5/6. The yield function

$$\Phi = \left| \frac{N_1}{134.5 \text{ N} - N_1^h} \right|^{2.04} + \left| \frac{N_3}{327.25 \text{ N} - N_3^h} \right|^{1.75} + \left| \frac{M_2}{123.375 \text{ N mm} - M_2^h} \right|^{1.93} - 1, \quad (3.2)$$

with the stress resultants N_1 for the shear force, N_3 for the axial force, M_2 for the bending moment, and the corresponding hardening contributions N_1^h , N_3^h , M_2^h , is accompanied by the kinematic hardening tensor

$$\mathbf{H} = \begin{bmatrix} 147.887 \text{ N} & 257.608 \text{ N} & 173.631 \text{ N mm} \\ & 442.276 \text{ N} & 254.883 \text{ N mm} \\ \text{sym.} & & 175.812 \text{ N mm}^2 \end{bmatrix}.$$

Further details on the elasto-plastic beam formulation can be found in [21, 9] and are not part of this contribution.

For an adequate model of the beam connections slight modifications to the beam geometry have been applied. In accordance with [13], the last elements in each beam are assumed purely elastic and

thickened by a factor of 1.25 for the stiffness calculation and thinned by a factor of 0.75 for the inertia calculation. Contact is implemented with friction-less sliding for linear two-node elements, following [25] for node-to-element contact. In the present two-dimensional set-up, only node-to-element contact is occurring, and the penalty parameter is set to 4×10^6 . For dynamic simulations a predictor-corrector approach with automatic step-size control using a Milne-device is employed.

The set-up for the beam-based FE analyses is shown in Figure 3.6. Notable differences to the solid-based analyses in Section 3.2 are the emulation of the impactor at the upper side by adding a virtual mass totalling 1.2 kg to the nodes on the impact surface, as well as the replacement of the lower plate by a “beam” acting as a spring-mass system emulating the baseplate.

4 Results

To ensure the comparability of the different models, they were in a first step compared to each other. For this comparison ARH samples of both lengths were used. The total force recorded at the back face is shown over the time for all three models in Figure 4.1. One should note here, that the stopping points

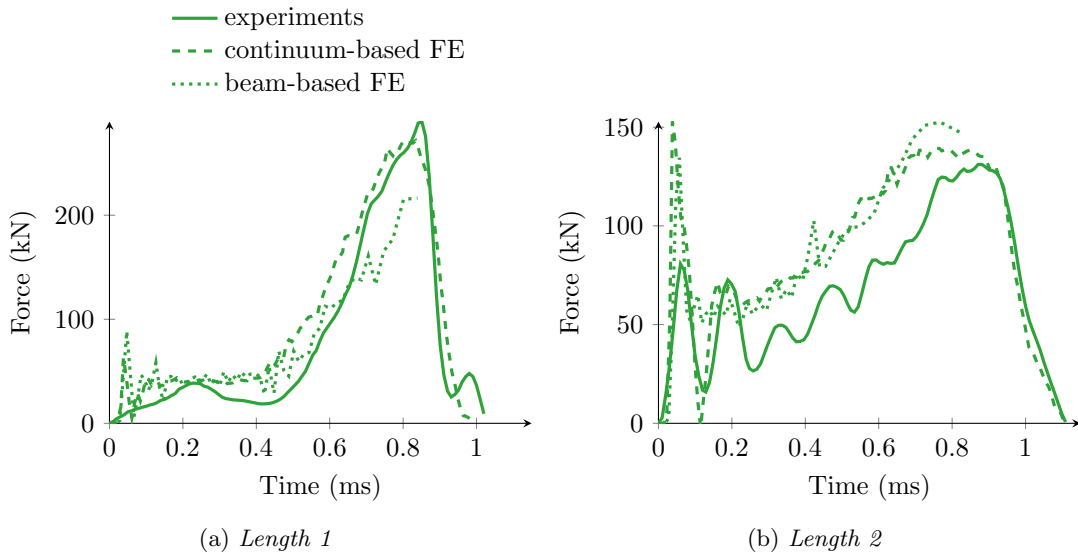


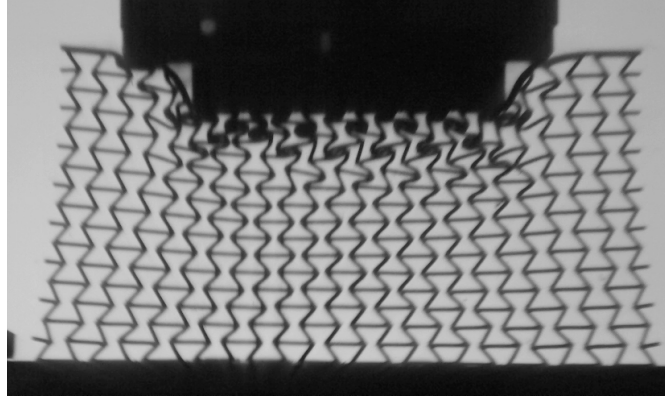
Figure 4.1: Comparison of the three models from Section 3 for ARH samples of (a) *Length 1* and (b) *Length 2*.

for all models are different, the experiment stops as soon as the total recorded force reaches 0 N, the continuum bases simulations are run for 1.3 ms and the beam-based simulations are run until the plunger velocity reaches 0 m s^{-1} .

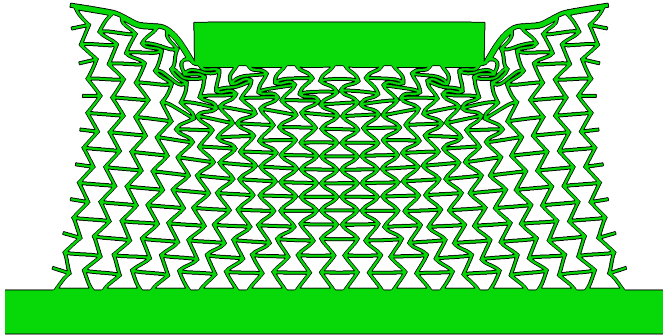
When comparing the force recordings, an overestimation of the forces in the FE models can be observed. Such overestimation can be explained by the fact, that in the experiment the backing is not infinitely stiff, whereas the simulations assume a fully rigid boundary condition. In the high-speed videos a flexing motion of the backplate is observed, despite the best efforts to obtain a stiff test set-up. This effect of overestimating the force levels is also seen for all other simulations of the experimental cases described in Table 4.1. The comparisons for other architectures are shown in B. In general all simulations show higher force levels as well as a higher initial peak. Both of these phenomena can be explained by the rigid backing assumed in the simulation.

As the over-prediction of the force is a phenomenon observed in the correlation of all simulations with their respective experimental cases and not severe, the simulations were deemed trustworthy for the assessment of differences in the behaviour of different architectures. However, it was decided to exclude the first 0.15 ms from the computation of integral measures, such as the average or maximum pressure, in order to exclude the effects of the higher initial peak.

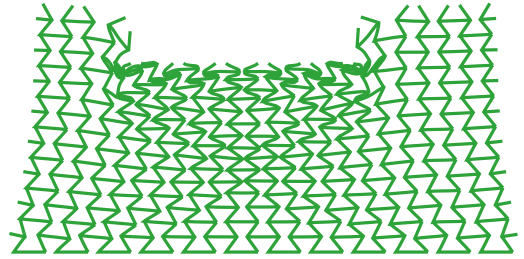
Further illustration for all models is provided in Figure 4.2. In this figure, the *Length 2* ARH sample is shown in all three investigation settings after 0.25 ms. Here, it is observable, that both FE models



(a) Experiment



(b) Continuum-based FE



(c) Beam-based FE

Figure 4.2: Comparison of the deformation of the *Length 2* ARH sample at 0.25 ms. (a) is showing the experimental image, (b) the continuum-based FE result, and (c) the beam-based FE result

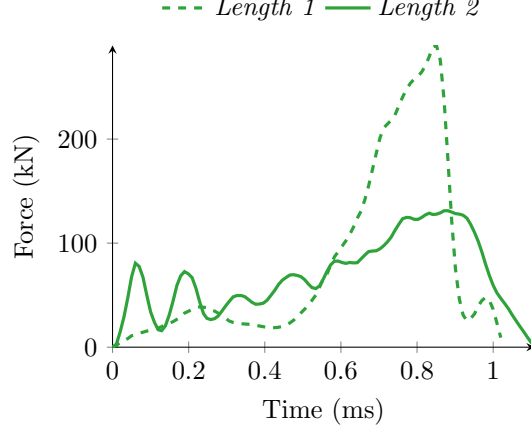


Figure 4.3: Experimental comparison of samples sizes for ARH.

Table 4.1: Recorded plunger velocities and masses.

	<i>Unit cell</i>		velocity (m s^{-1})	mass (g)
SM	ARH	<i>Length 1</i>	74	1202
	ARH	<i>Length 2</i>	71	1199
	ARH90	<i>Length 2</i>	72	1200
	CHW	<i>Length 2</i>	77	1199
	CHL	<i>Length 2</i>	73	1192
SS	ARH90	<i>Length 2</i>	76	1202
	CHW	<i>Length 2</i>	75	1199
	CHL	<i>Length 2</i>	75	1202

show a stronger indentation of the plunger compared to the experimental model, which is again explained by the higher forces due to the rigid backing. Other than that, despite not matching the deformation of each singular member in the lattice precisely, the overall deformation pattern between the experiments and the FE models match closely. Especially the start of the densification in the upper end of the sample as well as stronger densification around the edges of the strike-face are matched well. This confirms the assessment based on the total force recordings, that the models are comparable and valuable insights can be gained by exploring all three models available.

4.1 Experiments

The first set of experiments were conducted on the baseline ARH for both patch configurations, *Length 1* and *Length 2*. Both samples are displayed in an undeformed state in Figure 2.2. The corresponding recorded forces on the back-face are shown in Figure 4.3 over time measured from the initial contact between the plunger and the front face of the sample. In this figure it is observed that the initial part of the load is lower and more stable for the globally impacted sample of length 1. However, a large peak in the measured reaction force occurs after 0.8s, followed by a subsequent sharp drop to 0N. In contrast, the sample of *Length 2* exhibits a more dispersed impact load (and, consequently, impact energy) over time, maintaining a relatively constant transmitted force until the plunger is fully stopped. This comparison indicates that the surrounding material is crucial for the determination of the protection level of a structure. From the level of force, a localized impact is to be preferred in order to spread out the momentum transfer over a longer time resulting in a lower peak force throughout the time.

Subsequent experiments were conducted to compare other auxetic and non-auxetic architectures, as specified in Table 2.2, with the baseline ARH sample described above. The actual impact velocity and masses of each plunger were measured and are described in Table 4.1. In the first series, all four samples were designed to have the same initial elastic stiffness. Figure 4.4a shows the force summed up over the sensors plotted over time. Note that the force measurements due to the rebound of the plunger are not shown here as they are not the focus of this research. Both auxetic structures—ARH and ARH90 SS—show a distinct peak in the force curve. Upon examination of the high-speed videos, it can be observed that this correlates with the point at which the material is approaching complete densification.

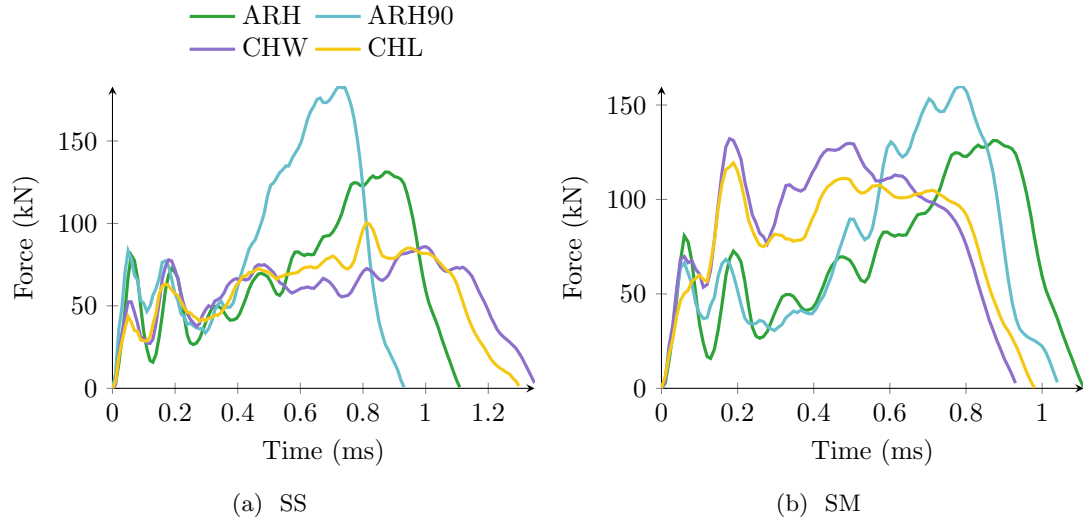


Figure 4.4: Force comparison for same (a) stiffness and (b) mass from the physical tests.

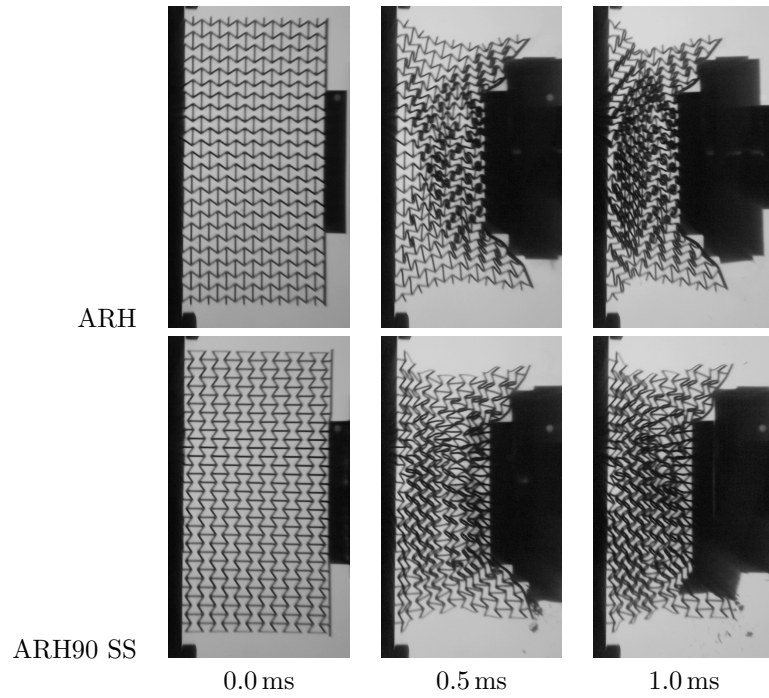


Figure 4.5: Stills from the high-speed recordings for both auxetic architectures.

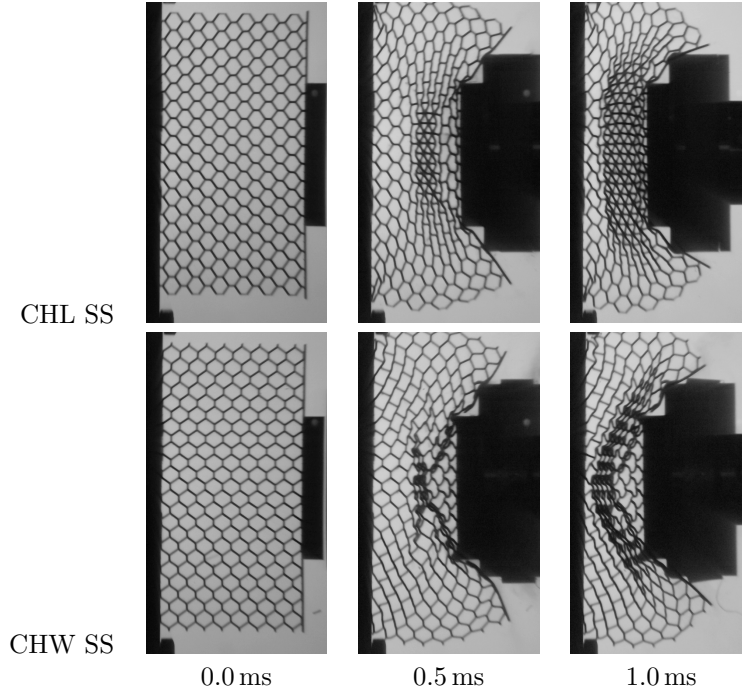


Figure 4.6: Stills from the high-speed recordings for both non-auxetic architectures.

Consequently, the subsequent deceleration of the plunger is solely due to material compression, requiring significantly greater forces compared to the lattice deformation in the earlier stages of the impact. Stills from the high speed videos of both— ARH and ARH90 SS—can be found in Figure 4.5. In contrast, the conventional honeycombs in both orientations do not show a distinct peak, but rather a slower deceleration at lower forces. Here, due to the positive Poisson’s ratio of the material, the structural members of the lattice are laterally pushed away from the impact location. This effect spreads the load over a larger area, as the laterally pushed away material deforms as well. This lateral deformation can be observed in Figure 4.6, where stills of the high speed videos throughout the deformation are shown for both CH SS configurations. The details of this spatial load distribution were investigated further in the numerical experiments.

Another comparison is undertaken with the lattices designed to exhibit a similar mass as the ARH baseline architecture. The corresponding measured forces over time are shown in Figure 4.4b. The ARH curve is the same as in Figure 4.4a, as this architecture is used as baseline. The rotated auxetic structure (ARH90) possesses a less distinct, more flat peak. The CH curves are higher than the Figure 4.4a curves, due to the higher stiffness resulting from the thicker beams due to the equivalent mass design target. These higher initial stiffnesses are recorded Table 2.1. Both conventional honeycombs show the same peak-less behaviour as described for Figure 4.4a, but at a higher level.

4.2 Continuum-based FE

In order to enhance understanding of the physical experiments, numerical simulations were conducted as detailed in Section 3.2. Simulations allow the extraction of data from any point, without any physical restrictions and effects on the results. Of special interest in the context of impact mitigation is not only the total force transmitted to the back of the protective structure, but also the pressure distribution over the back face. For this the average and maximum pressure onto locations of the back-face were computed over time, with the exclusion of the first 0.15 ms as described above. The average pressure over the back-face is shown in Figure 4.7b for all architectures with the same stiffness. This average pressure distribution in Figure 4.7b shows that the auxetic densification leads to a concentration of the forces on a smaller area, resulting in a higher pressure onto the protected structure at the back of the plate. It should be mentioned that the highest average pressures for the non-auxetic structures are at the edges of the lattice structure, indicating, that the length of the CH samples is not sufficient to spread the

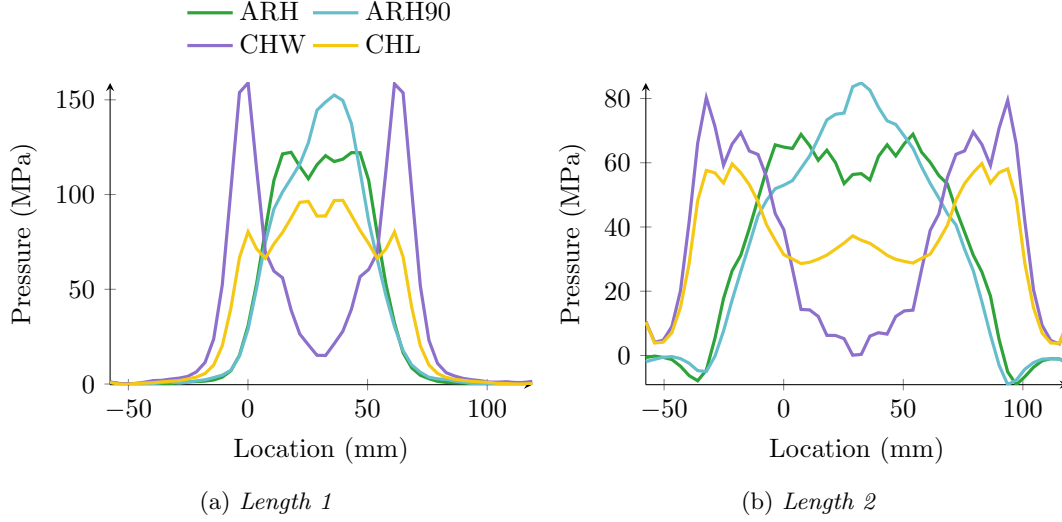


Figure 4.7: Comparison of the average pressure over the back-face for the SS configuration using the continuum-based model.

load effectively. This is emphasized by the observation of similar pressure distributions in the samples with only the single width, as shown in Figure 4.7a. Here the auxetic samples concentrate the force in the centre, resulting in higher pressures onto the backside, whereas the non-auxetic samples distribute force to the boundaries. A point to note here is that the CHL architecture also shows a slight peak in the centre of the structure. This can be explained by the deformation of this unit cell leading to auxetic behaviour, as shown previously [8].

Similar observations can be made upon examination of the maximum pressure on the backside. For the *Length 2* samples, this is shown in Figure 4.8b and for the *Length 1* samples in Figure 4.8a. The load spreading effect is less distinct, but still observable in the plot showcasing the *Length 2* samples. In the plot with the *Length 1* samples the discussed effects of CHL architectures becoming auxetic is even more prevalent. This is also observable in the deformation throughout the physical experimentation in Figure 4.6. Similar behaviour can be seen in C for the SM configuration.

4.3 Beam-based FE

Using the more efficient beam-based model, investigations into wider samples are available for fast computation. For this, all SS samples reported in Table 2.1 as *Length 2* are elongated in the horizontal direction again, leading to a doubling of the unit cells in x -direction, whilst all other boundary conditions are kept the same. In Figure 4.9, again, the recorded total force on the back-face is shown over time. The solid lines represent the samples of *Length 2*, whereas the samples of *Length 4* are shown in dashed lines. No substantial difference between the two sample widths can be observed, indicating together with the results from the continuum-based simulation, that the increase in length does not affect the total force transmission profile, but only the pressure distribution. The same accordance between the configurations can be found when examining the results for the SM configurations in C.

Another additional study enabled by the structural FE modelling is the change of the unit cell size. For this, unit cell sizes are halved and the number of unit cells reported in Table 2.1 in each direction is doubled in order to have the same outer measures for each sample. This leads to a quadrupling of the overall number of unit cells whilst keeping the mass the same. In Figure 4.10 the comparison between the two investigated sizes is plotted for the samples of *Length 2* in the SS configuration. In this graph, the force on the back-face is shown for all different unit cells over the time. The default unit cells of *Size 1* are shown in solid lines, whereas the adapted unit cells of *Size 0.5* are depicted with dashed lines. In the graph it can be observed, that reducing the size of the unit cells leads to higher forces being transmitted through the lattice. This scale effect is also present in the *Length 1* samples, as shown in Figure 4.11. The *Length 1* samples also exhibit visually the reason for this behaviour as showcased in Figure 4.12. In the figure the deformation of the lattices are showcased at 0.05 ms, 0.10 ms and 0.15 ms. In the images

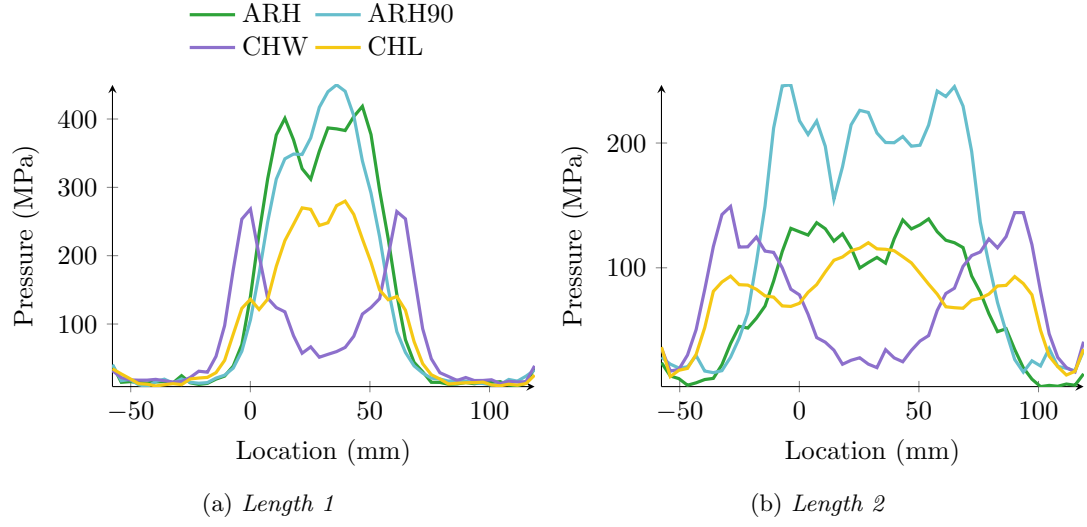


Figure 4.8: Comparison of the maximum pressure over the back-face for the SS configuration using the continuum-based model.

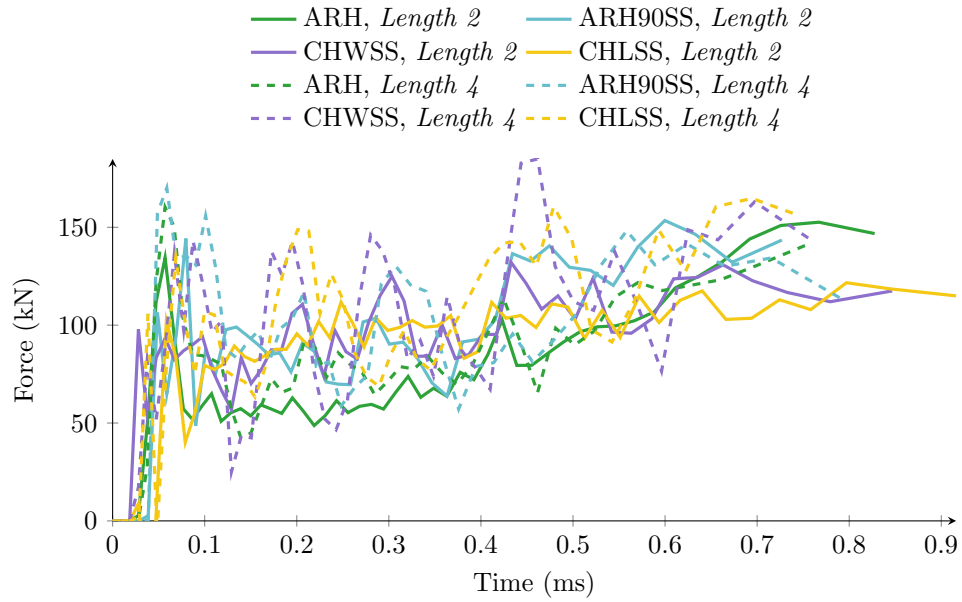


Figure 4.9: Comparison of longer samples for the SS configuration.

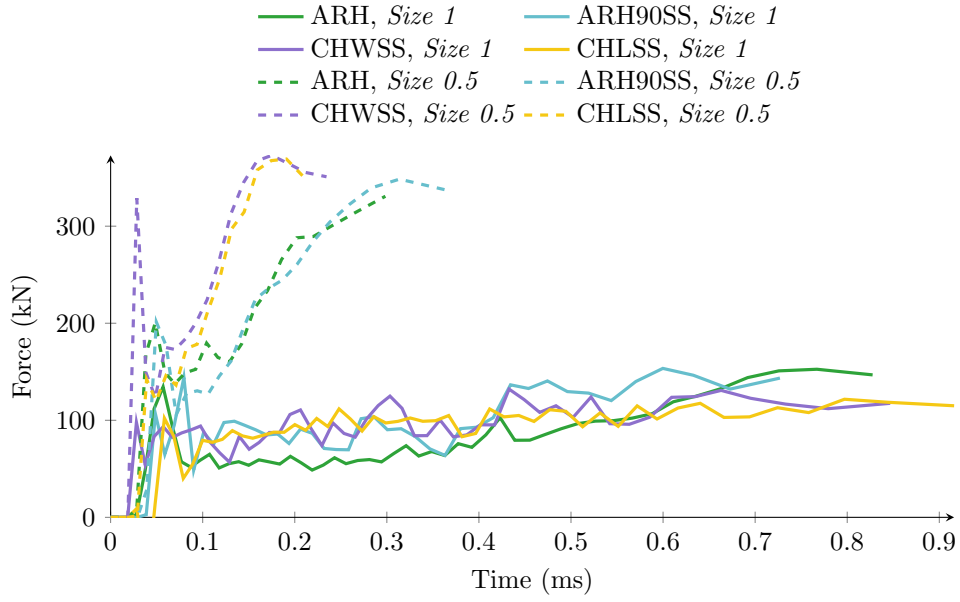


Figure 4.10: Comparison of smaller unit cells for the SS configuration of *Length 2* using the beam-based model.

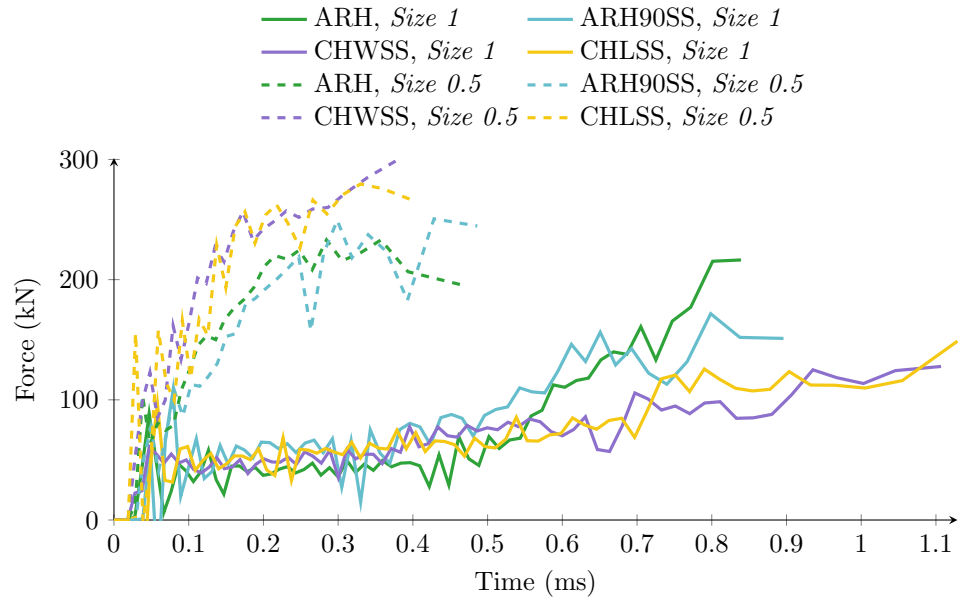
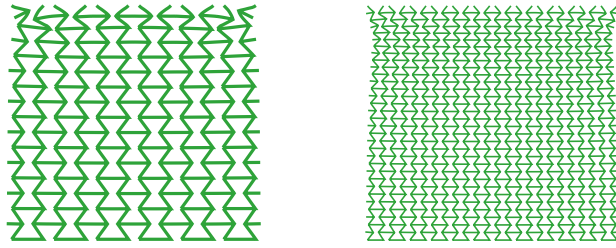
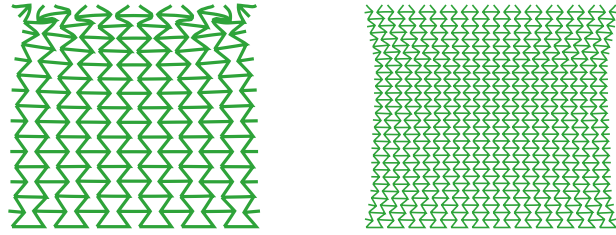


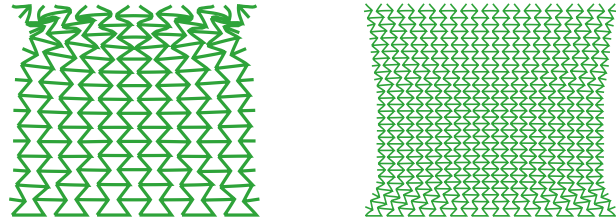
Figure 4.11: Comparison of smaller unit cells for the SS configuration of *Length 1* using the beam-based model.



(a) 0.05 ms



(b) 0.10 ms



(c) 0.15 ms

Figure 4.12: Deformation plots showcasing ARH samples of *Length 1* for different unit cell sizes.

at 0.05 ms the start of the collapse of the top row of unit cells is visible in the *Size 1* sample on the left. On the right, in the *Size 0.5* sample, no such phenomenon is occurring, rather the start of a progressive wave of lateral contracting can be seen, which can be seen progressing downwards in the middle and lower picture, at 0.10 ms and 0.15 ms, respectively. The *Size 1* sample, in the left column, on the other hand shows no such wave, but rather the further (plastic) collapse of the topmost row of unit cells. The original size sample therefore enters their plastic collapse stress plateau before full densification, whereas the *Size 0.5* samples are able to deform elastically over a wider range, thus offering to absorb the impact energy faster to the consequence of also transmitting higher forces. This behaviour can be observed in the SM configuration samples in C as well.

5 Discussion & Conclusion

Looking back at the experimental campaign that resulted in the transmitted force onto the underlying material after a localized impact, it is first observed, that higher stiffness leads to more direct force transmission. Whilst all samples are able to absorb the impact energy, this showcases CH structures outperforming ARH structures when the main concern is energy absorption in a limited area with a given weight, as they were able to do so in a shorter time. Reducing this stiffness allows the energy absorption to be spread out over a longer time for the CH samples, that are not stiffening unlike the ARH samples. This spread of the energy absorption results in lower total force levels on the back-face of the protective structure, potentially reducing the load on the precious load being protected by the lattice, which is beneficial for the protection as reported by Gupta and Ding [11]. While under blast loading, the more compliant edges can help distribute the force, as reported by [2], due to the external load spread inherent to a blast. Under ballistic impact, the load is naturally concentrated and thus negative Poisson's ratio and the corresponding densification and stiffening lead to a further concentration of the load.

These finding are reinforced by the additional numeric studies into the load spread on the back-face of the material. Here, it can be summarized, that in the given context, a negative Poisson's ratio in the material leads to higher peaks in the loading on the backside of the protective structure also in space. This effect is especially pronounced when comparing the structures with comparable stiffness. When looking at structures with equivalent mass, the higher stiffness of the conventional architectures has a stronger effect compared to its load distribution effect. Overall it has been shown, that higher stiffness improves the energy absorption qualities in a confined space, but also lead to worse protective properties due to higher force peaks onto the back-face of the protective structure.

Thus, it can be concluded that the impact mitigation efficacy of auxetic materials only appears in the case, where energy absorption and the need to limit the deformation of the protective structure at a given stiffness are the main measures. In other cases, especially when the spreading of the load or lightweight design of the protective structures are of interest, auxetic structures are not able to spread the load of a localized impact and perform worse than conventional honeycombs when considering equal mass even in pure energy absorption measures.

Lastly, investigations with different scenarios as well as size effects using faster, albeit less accurate, beam-based simulations were conducted to explore other patch configurations. In the first trial, the investigation into even longer samples lead to no significant difference between patches of *Length 2* and *Length 4*, emphasizing the assumption that the earlier investigations are representative of a local impact onto a wide structure. The contradiction with [8], where wider samples are suggested to be required for assessment of the performance of the structure, can be explained by the limitation to elasticity in that study, whereas plasticity, especially in this dynamic environment, can lead to higher localization, which prevents further spreading of the load. To this end as well, different unit cell sizes were compared in another investigation. Here a significant effect was observed, as the plastic collapse of the unit cells observed in the previous trials, was largely mitigated with smaller unit cells. This emphasizes the fact that, when designing protection concepts, one cannot use scaled tests, but instead full-scale (numerical or physical) tests are paramount to understand the full behaviour of the given structure.

This full-scale, physical testing campaign is needed to ensure all physical phenomena have been considered. Physical tests, especially when considering ballistic impact studies, require tremendous effort and resources to be conducted and have, as was demonstrated, difficulties to ensure the boundary conditions envisioned are met with certainty. To this end numerical tests enable faster and less resource-intensive

assessment with properly idealized boundary conditions. In numerical investigations, the placement of sensors is not a matter of physical practicality, but rather of limiting the amount of data, enabling a better understanding of the inside processes in the material and the detailed interactions with the surrounding structure. As there are different levels of refinement and discretization of structures, one needs to make good use of all available resources in order to speed up the design and research process and to further facilitate the understanding and development of protection concepts.

Summarizing the findings of this contribution, the existing primer of literature, that auxetics perform better for impact mitigation, has been challenged by data of force distribution, where simple non-auxetic honeycombs were shown to exhibit better performance in multiple configurations. Also, the simple use of energy absorption as assessment tool for impact mitigation is insufficient in cases where not only the total energy absorbed by the structure is relevant for the safety of the protected structure, but also the distribution of the transmitted forces. To this end various numerical tools were shown to be effective supplements to the physical experiments in order to gain a deeper understanding, which can be used for the design of protective structures and further research on this topic. For this further research, the assessment of different auxetic and non-auxetic structures will be valuable for the community to ensure that findings can be generalized, as well as a deeper investigation into the interdependency of the inelastic, strain-rate dependent response and the size of unit cells in architected materials.

Author contributions

T. Gärtner: Conceptualization; Data curation; Investigation; Methodology; Software; Validation; Visualization; Writing – original draft; **R. Dekker:** Conceptualization; Data curation; Investigation; Methodology; Software; Writing – review & editing; **D. van Veen:** Conceptualization; Investigation; Methodology; Resources; Writing – review & editing; **S.J. van den Boom:** Conceptualization; Investigation; Methodology; Project administration; Supervision; Writing – review & editing; **L. Amaral:** Conceptualization; Investigation; Methodology; Project administration; Validation; Writing – review & editing;

Declaration of competing interests

The authors declare that they have no known competing financial interests or personal relationships that could have appeared to influence the work reported in this paper.

Acknowledgements

The authors would like to acknowledge Etienne van Daelen, Peter van Ierschot, and Reinoud van de Kastele for technical support in the experimental campaign, Koen Winters for support with the technical drawings and fruitful discussions, as well as André Diederer, Lex Niessen, Bert Sluys, and Jaap Weerheijm for fruitful discussions.

Funding

The research was funded by The Netherlands Ministry of Defence.

Data availability

Data will be made available upon request.

A Construction Drawings

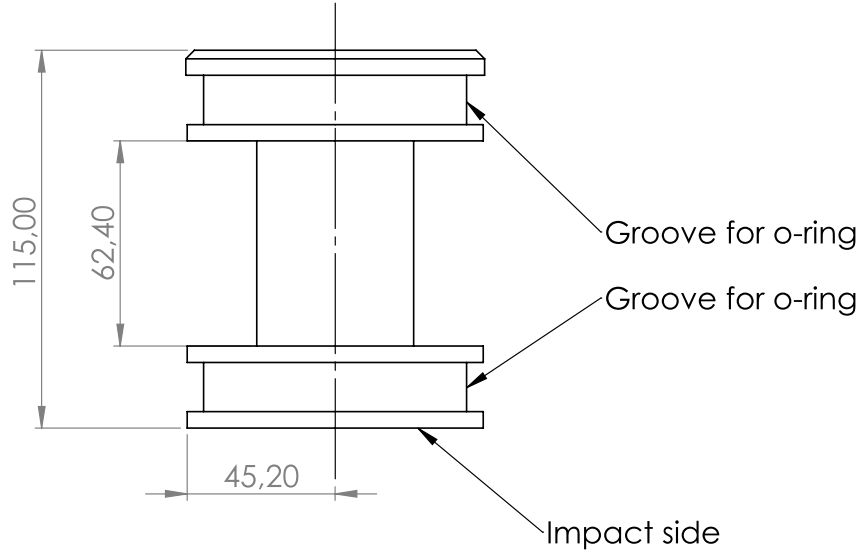


Figure A.1: Plunger with main dimensions in mm.

B Experimental and Numerical Comparisons

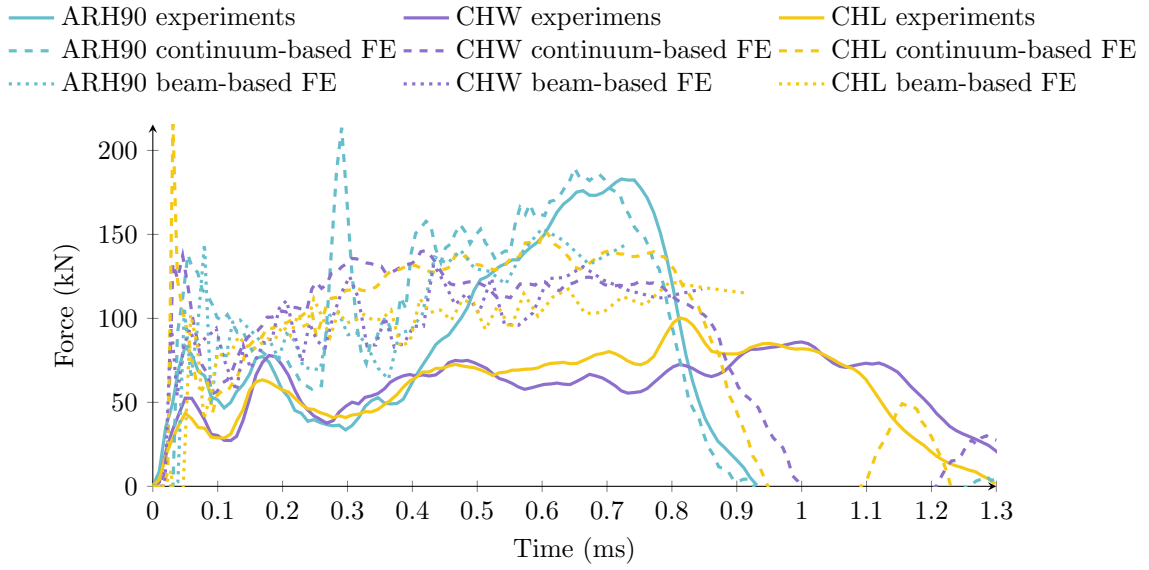


Figure B.1: Comparison of experimental and numerical results for the SS configuration.

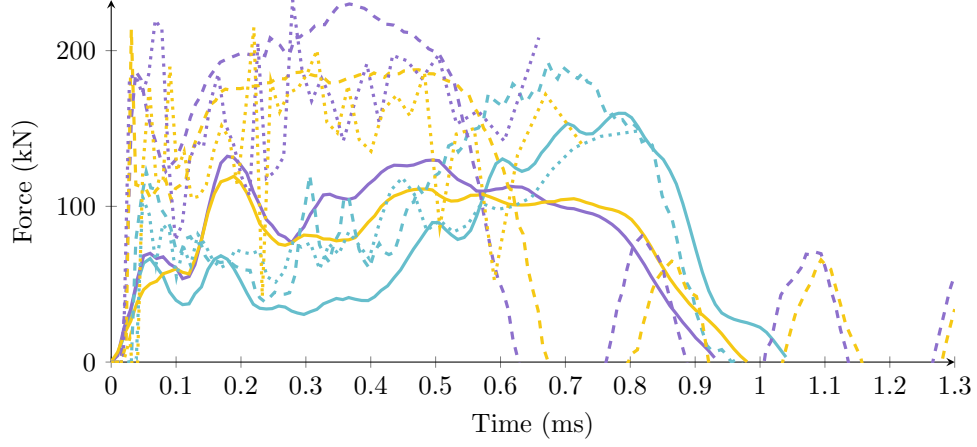


Figure B.2: Comparison of experimental and numerical results for the SM configuration.

C Structural Samples Comparisons

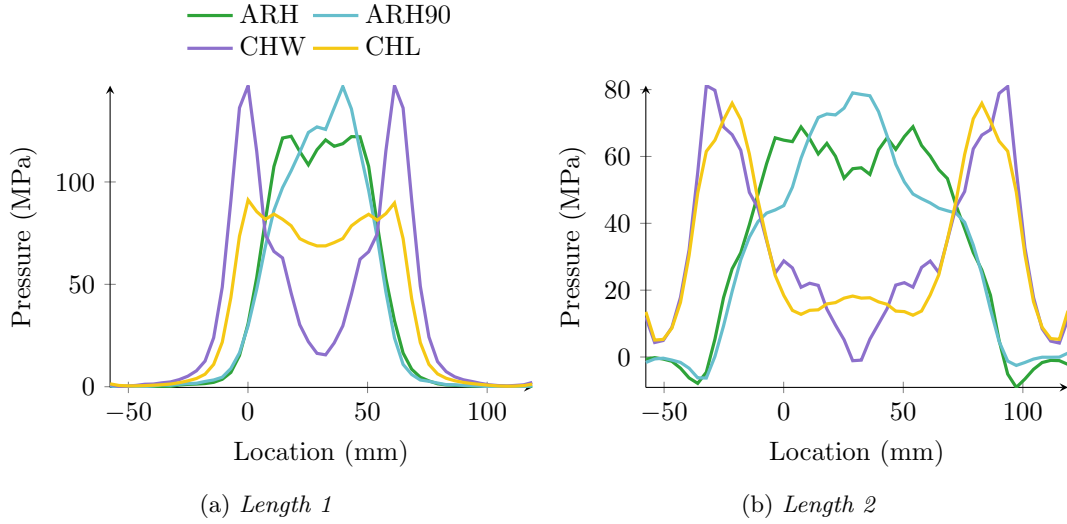


Figure C.1: Comparison of the average pressure over the back-face for the SM configuration using the continuum-based model.

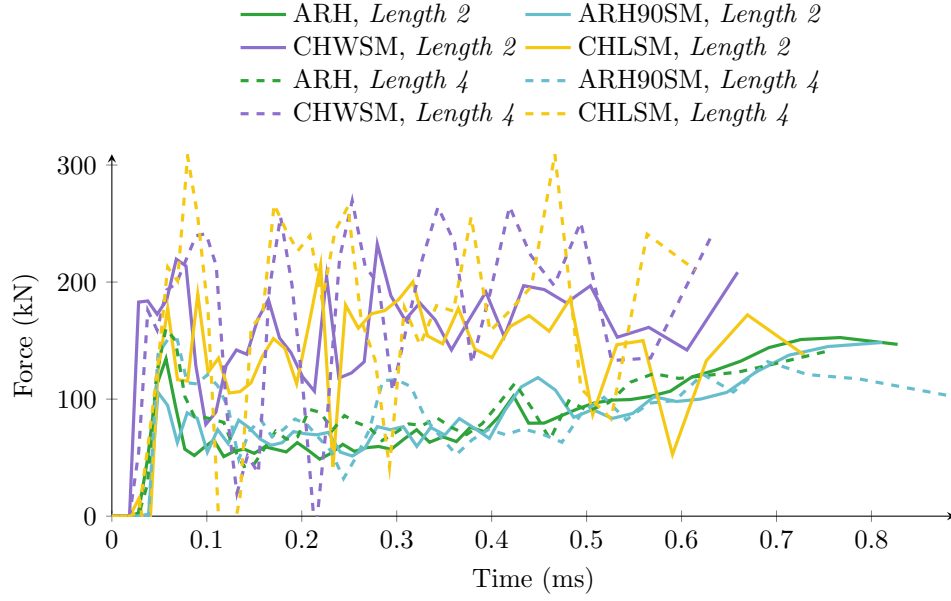


Figure C.2: Comparison of wider samples for the SM configuration using the beam-based model.

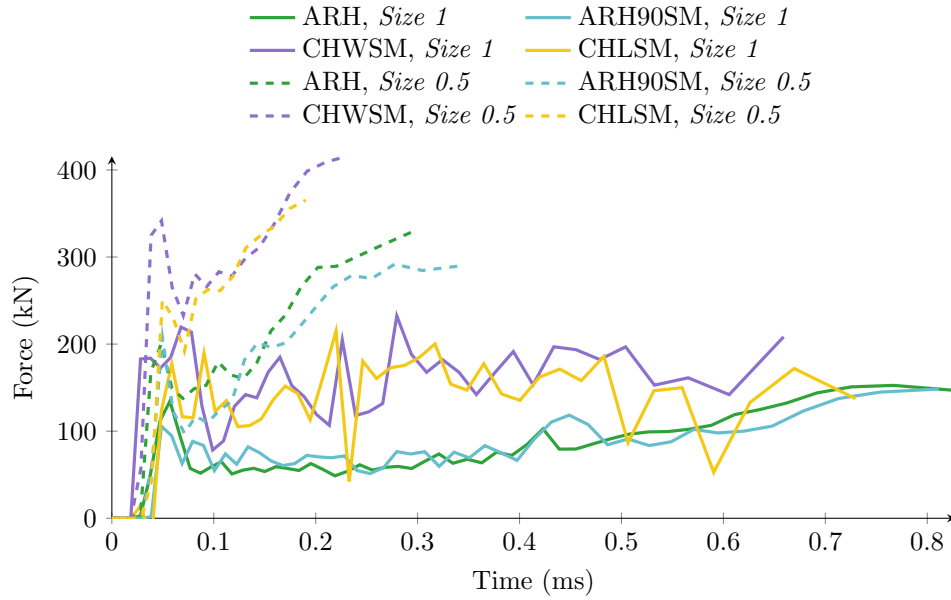


Figure C.3: Comparison of smaller unit cells for the SM configuration using the beam-based model.

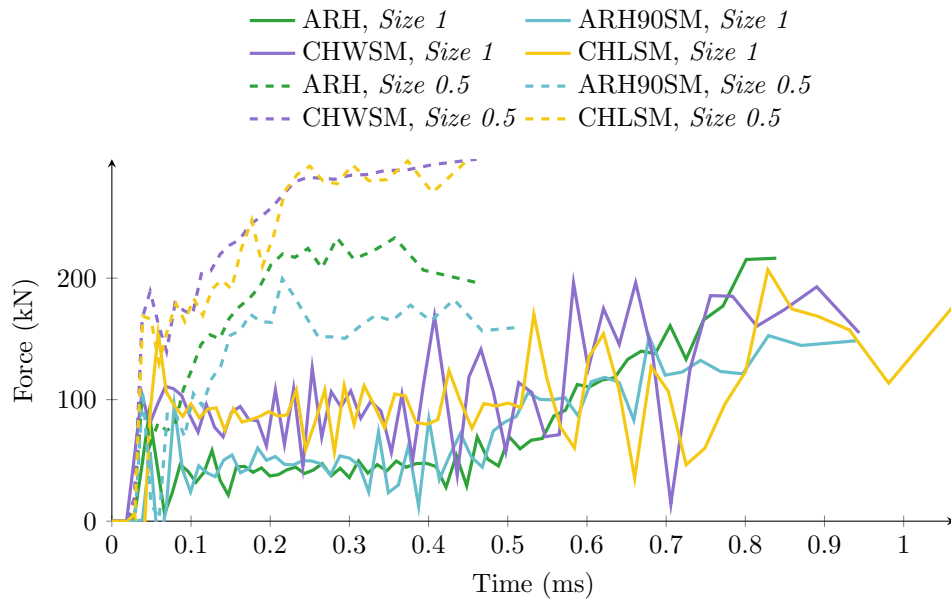


Figure C.4: Comparison of smaller unit cells for the SM configuration *Length 1* using the beam-based model.

References

- [1] R. P. Bohara et al. “Anti-blast and -impact performances of auxetic structures: A review of structures, materials, methods, and fabrications”. In: *Engineering Structures* 276 (2023). DOI: 10.1016/j.engstruct.2022.115377.
- [2] R. P. Bohara et al. “Performance of an auxetic honeycomb-core sandwich panel under close-in and far-field detonations of high explosive”. In: *Composite Structures* 280 (2022). DOI: 10.1016/j.compstruct.2021.114907.
- [3] M. A. Crisfield and G. Jelenić. “Objectivity of strain measures in the geometrically exact three-dimensional beam theory and its finite-element implementation”. In: *Proceedings of the Royal Society of London. Series A: Mathematical, Physical and Engineering Sciences* 455.1983 (1999), pp. 1125–1147. DOI: 10.1098/rspa.1999.0352.
- [4] R. Critchley et al. “A review of the manufacture, mechanical properties and potential applications of auxetic foams”. In: *physica status solidi (b)* 250.10 (2013), pp. 1963–1982. DOI: 10.1002/pssb.201248550.
- [5] Dynaflow Research Group. *JIVE*. Version 3.0. Apr. 1, 2021. URL: <https://dynaflow.com/software/jive/>.
- [6] B. K. Fink. “Performance Metrics for Composite Integral Armor”. In: *Journal of Thermoplastic Composite Materials* 13.5 (2000), pp. 417–431. DOI: 10.1106/fr01-t33w-jpd0-vfh3.
- [7] R. Galea Mifsud et al. “Auxetics and FEA: Modern Materials Driven by Modern Simulation Methods”. In: *Materials (Basel)* 17.7 (2024). DOI: 10.3390/ma17071506. URL: <https://www.ncbi.nlm.nih.gov/pubmed/38612021>.
- [8] T. Gärtner et al. “Geometric effects on impact mitigation in architected auxetic metamaterials”. In: *Mechanics of Materials* 191 (Nov. 2024). DOI: 10.1016/j.mechmat.2024.104952.
- [9] T. Gärtner et al. “A strategy for scaling the hardening behavior in finite element modelling of geometrically exact beams”. In: *Computational Mechanics* (2024). DOI: 10.1007/s00466-024-02572-3.
- [10] L. J. Gibson et al. “The mechanics of two-dimensional cellular materials”. In: *Proceedings of the Royal Society of London. A. Mathematical and Physical Sciences* 382.1782 (1997), pp. 25–42. DOI: 10.1098/rspa.1982.0087.
- [11] Y. M. Gupta and J. L. Ding. “Impact load spreading in layered materials and structures: concept and quantitative measure”. In: *International Journal of Impact Engineering* 27.3 (2002), pp. 277–291. DOI: 10.1016/S0734-743X(01)00051-3.
- [12] G. R. Johnson and W. H. Cook. “Fracture characteristics of three metals subjected to various strains, strain rates, temperatures and pressures”. In: *Engineering Fracture Mechanics* 21.1 (1985), pp. 31–48. DOI: 10.1016/0013-7944(85)90052-9.
- [13] K. Kappe et al. “Multi-objective optimization of additive manufactured functionally graded lattice structures under impact”. In: *International Journal of Impact Engineering* (2023). DOI: 10.1016/j.ijimpeng.2023.104789.
- [14] W. Liu et al. “In-plane dynamic crushing of re-entrant auxetic cellular structure”. In: *Materials & Design* 100 (2016), pp. 84–91. DOI: 10.1016/j.matdes.2016.03.086.
- [15] C. Qi et al. “Impact and close-in blast response of auxetic honeycomb-cored sandwich panels: Experimental tests and numerical simulations”. In: *Composite Structures* 180 (2017), pp. 161–178. DOI: 10.1016/j.compstruct.2017.08.020.
- [16] E. Reissner. “On finite deformations of space-curved beams”. In: *ZAMP Zeitschrift für angewandte Mathematik und Physik* 32.6 (1981), pp. 734–744. DOI: 10.1007/bf00946983.
- [17] X. Ren et al. “Auxetic metamaterials and structures: a review”. In: *Smart Materials and Structures* 27.2 (2018). DOI: 10.1088/1361-665X/aaa61c.
- [18] K. K. Saxena, R. Das, and E. P. Calius. “Three Decades of Auxetics Research – Materials with Negative Poisson’s Ratio: A Review”. In: *Advanced Engineering Materials* 18.11 (2016), pp. 1847–1870. DOI: 10.1002/adem.201600053.

- [19] J. C. Simo. “A finite strain beam formulation. The three-dimensional dynamic problem. Part I”. In: *Computer Methods in Applied Mechanics and Engineering* 49.1 (1985), pp. 55–70. DOI: 10.1016/0045-7825(85)90050-7.
- [20] J. C. Simo and L. Vu-Quoc. “A three-dimensional finite-strain rod model. part II: Computational aspects”. In: *Computer Methods in Applied Mechanics and Engineering* 58.1 (1986), pp. 79–116. DOI: 10.1016/0045-7825(86)90079-4.
- [21] Smriti, A. Kumar, and P. Steinmann. “A finite element formulation for a direct approach to elasto-plasticity in special Cosserat rods”. In: *International Journal for Numerical Methods in Engineering* (2020). DOI: 10.1002/nme.6566.
- [22] T. J. Spradlin. “Process Sequencing for Fatigue Life Extension of Large Scale Laser Peened Components”. Dissertation. Wright State University, 2011. URL: https://corescholar.libraries.wright.edu/etd_all/495/.
- [23] D. Umbrello, R. M’Saoubi, and J. C. Outeiro. “The influence of Johnson–Cook material constants on finite element simulation of machining of AISI 316L steel”. In: *International Journal of Machine Tools and Manufacture* 47.3-4 (2007), pp. 462–470. DOI: 10.1016/j.ijmachtools.2006.06.006.
- [24] O. Weeger et al. “Inelastic finite deformation beam modeling, simulation, and validation of additively manufactured lattice structures”. In: *Additive Manufacturing Letters* 4 (2023). DOI: 10.1016/j.addlet.2022.100111.
- [25] P. Wriggers and J. C. Simo. “A note on tangent stiffness for fully nonlinear contact problems”. In: *Communications in Applied Numerical Methods* 1.5 (1985), pp. 199–203. DOI: 10.1002/cnm.1630010503.