

Correcting stress–strain curves from large-deformation V-notched rail shear tests

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Abstract

Stress–strain curves obtained from V-notched rail (VNR) shear tests can become increasingly inaccurate at large deformation as the principal directions evolve and the ligament deformation becomes non-uniform. This study combines PEEK VNR experiments at 180 °C, digital image correlation (DIC), simple-shear kinematics and finite element analysis to quantify and correct this error. The comparison shows that the main discrepancy arises from stress rather than strain calculation: the calculated strain remains close to the ligament-centre response, whereas the stress error increases to about 12% at an effective strain of approximately 0.10. The main advance is to correct the full large-deformation VNR stress–strain response before constitutive identification. This extends the correction beyond individual test properties such as modulus or strength. A two-step stress correction is therefore developed. The first step uses an ROI-averaged reference to address the contribution associated with principal-direction change. The second uses a ligament-centre reference to address the remaining effect associated with non-uniform ligament deformation. After correction, the numerical curves closely follow the prescribed material response and the experimental VNR curve shows substantially improved agreement with the tensile reference. The correction also reduces the apparent hardening identified from the VNR response and improves subsequent strain prediction. Additional numerical cases for PEEK at 240 °C and PA6 at 160 °C show a similar reduction in stress error.

Keywords

Mechanical testing; Large deformation; Thermoplastic polymers; V-notched rail shear; Stress correction; Constitutive identification

1. Introduction

V-notched rail (VNR) and rail shear tests are established methods for material shear characterisation [1–4], while the Iosipescu configuration provides another widely used notched shear method [5]. In these tests, the measured resultant load is combined with a local strain measurement or strain field to construct a stress–strain response. At small deformation, conventional shear assumptions provide a straightforward conversion. At large deformation, however, finite simple-shear kinematics cause the principal directions to evolve [6,7], while polymer shear experiments show increasingly heterogeneous deformation [8–10]. Similar behaviour has been reported for other thermoplastic systems under large shear deformation [11,12]. Large-deformation studies of polymers and nonlinear solids further demonstrate that local deformation can depart markedly from small-deformation idealisations [13–15]. Numerical analyses of notched shear specimens similarly show that the measured response depends on specimen geometry and non-uniform local stress and strain fields [16–18], with constituent and interface effects providing further complexity in composite systems [19,20]. Consequently, retaining the same load-to-stress conversion as deformation increases can produce a deformation-dependent stress error. Because this error changes with deformation rather than introducing a constant offset, it can alter the apparent shape and hardening of the extracted stress–strain curve.

This issue is important for constitutive modelling. Thermoplastics exhibit strong temperature-, rate- and deformation-dependent behaviour, so constitutive models rely on accurate experimental descriptions of yielding, softening and hardening [21–24]. PEEK studies have shown pronounced changes in post-yield behaviour with temperature and strain rate [25,26], while tensile, impact and other loading studies demonstrate further sensitivity to the imposed thermal and mechanical conditions [27–29]. More generally, physically motivated polymer models associate large-strain behaviour with molecular-network stretching, rate-dependent plastic flow and evolving microstructural resistance [30–32], with similar finite-strain and time-dependent behaviour reported for other polymers [33,34]. Recent PEEK studies continue to refine this description through wide-temperature constitutive modelling and coupled temperature-, rate- and damage-dependent formulations [35–37], together with atomistic analysis [38]. Related work has also advanced physics-based, data-assisted and multiaxial constitutive descriptions of polymer systems [39–42]. A deformation-dependent bias

introduced during experimental stress calculation can therefore be absorbed into the fitted constitutive response and subsequently affect numerical prediction.

Two mechanisms are particularly relevant to VNR testing. The first is principal-direction change. Unlike the fixed principal directions of the small-deformation symmetric-shear idealisation, those in simple shear evolve continuously with deformation [6–9]. Recent full-field comparisons of standard shear methods have also quantified angular changes and rigid-body motion during large shear deformation, including V-notched configurations [43]. In a VNR specimen, the machine measures the resultant load across the ligament, whereas strain can be evaluated locally or over a selected region. The relationship between this load and the local principal stress state therefore changes as the principal directions evolve. The second mechanism is non-uniform ligament deformation. DIC makes this directly visible by providing spatially resolved displacement and strain fields [44–46], including under demanding measurement conditions [47–50]. Spatial resolution, noise and processing choices can influence the derived fields [51,52], while recent phase-augmented, physics-informed and uncertainty-aware approaches further emphasise measurement quality [53–55]. Full-field measurements have consequently been used to quantify localisation and heterogeneous deformation [56,57] and to support inverse identification in polymer systems [58,59]. Thus, once the ligament response becomes heterogeneous, a centre strain and an ROI-averaged strain need not represent the same deformation state.

Previous shear studies have recognised these effects and proposed modified specimen designs or numerical correction factors to improve measurements from non-uniform shear tests [10,16–18]. Recent VNR fixture development has also targeted in-plane and out-of-plane specimen rotation and assessed shear-field uniformity using DIC [60]. Taheri-Behrooz and Moghaddam [18], in particular, analysed the nonlinear VNR response and proposed corrections for shear-property determination, while related work shows that material heterogeneity can further affect the measured shear response [20]. These studies establish that numerical information can improve quantities extracted from notched shear tests. However, these approaches primarily target individual shear properties such as modulus or strength. The unresolved issue is the deformation-dependent error in the full VNR stress–strain response used for constitutive identification. If the stress-calculation error grows with deformation, it changes the curve shape from which material hardening parameters are identified. The consequence can therefore propagate beyond the reported test property into the calibrated constitutive model and subsequent simulation.

This problem is closely related to the broader development of full-field constitutive identification. Reviews and early inverse approaches established how heterogeneous deformation fields can be used to identify material parameters [61–63], while later studies compared identification strategies and developed the Material Testing 2.0 framework [64–66]. Sensitivity-based virtual fields extended this approach to nonlinear and anisotropic behaviour [67–70]. General optimisation frameworks, information metrics and Bayesian design have since been developed to improve parameter identification [71–73], alongside work on information-rich specimen design, multi-sample integration and constitutive-model selection [74–76]. Direct, multi-data and thermomechanical approaches exploit additional field information [77–80], while improved field processing has been used to reduce identification bias [81,82]. Recent work further addresses FEMU practice, identification uncertainty and load-path selection [83–85]. Recent studies have extended full-field identification to heterogeneous material properties, anisotropic plasticity, and high-throughput or direct characterisation approaches [86–90]. Bayesian, machine-learning and topology-optimisation approaches provide further routes for data-informed mechanics and parameter identification [91–94], while recent studies address spatial variability, noisy measurements and viscoelastic identification [95–97]. FEMU and full-field methods have also been extended to cyclic response and polymers [98–100], physics-informed and spatially varying identification [101,102], and strongly localised post-necking deformation [103]. These studies primarily address how material parameters are obtained from measured deformation fields. In VNR testing, however, an additional source of error can arise earlier, when the measured resultant load and either a local or ligament-averaged strain are converted into the stress–strain curve used for constitutive identification.

The specific advance of this study is to correct the full large-deformation VNR stress–strain response before constitutive identification. The two-step procedure addresses the contributions of principal-direction change and non-uniform ligament deformation in sequence. The study first quantifies the deformation-dependent error using local and ROI-averaged strain measures. It then examines the two mechanisms using kinematic analysis and finite element simulation and develops the correction for both local SG-type and DIC-based calculations. Finally, the effect of the correction on identified hardening parameters and subsequent numerical predictions is evaluated, with additional numerical cases for PEEK at 240 °C and polyamide 6 (PA6) at 160 °C.

The remainder of the paper is organised as follows: Section 2 describes the VNR experiments and baseline stress–strain conversion; Section 3 examines the two sources of calculation error; Section 4 establishes and assesses the finite element model; Section 5 develops and evaluates the correction and its effect on constitutive identification; and Section 6 summarises the main conclusions.

2. Experimental shear characterisation

VNR experiments were first used to obtain the load and DIC strain field during large shear deformation and to calculate the effective stress–strain response of PEEK at 180 °C. This response was compared with previously reported tensile data for the same material [26]. Finite element analysis was then used to obtain the local stress and strain quantities needed to identify the source of the difference and to develop the stress correction.

2.1. Materials and specimens

PEEK sheets of 5.5 mm thickness were supplied by Ai Engineering Plastics & Laminates, UK. PEEK is a semicrystalline thermoplastic used in structural components and as a matrix material in polymer composites. To reduce the distortion observed with the standard ASTM D7078 specimen [1], the ligament length was shortened to 15.68 mm. Fig. 1 shows the modified geometry, gripping arrangement and experimental setup. Spacer blocks were used to align the ligament with the loading axis, and the grip bolts were tightened to restrain out-of-plane twisting.

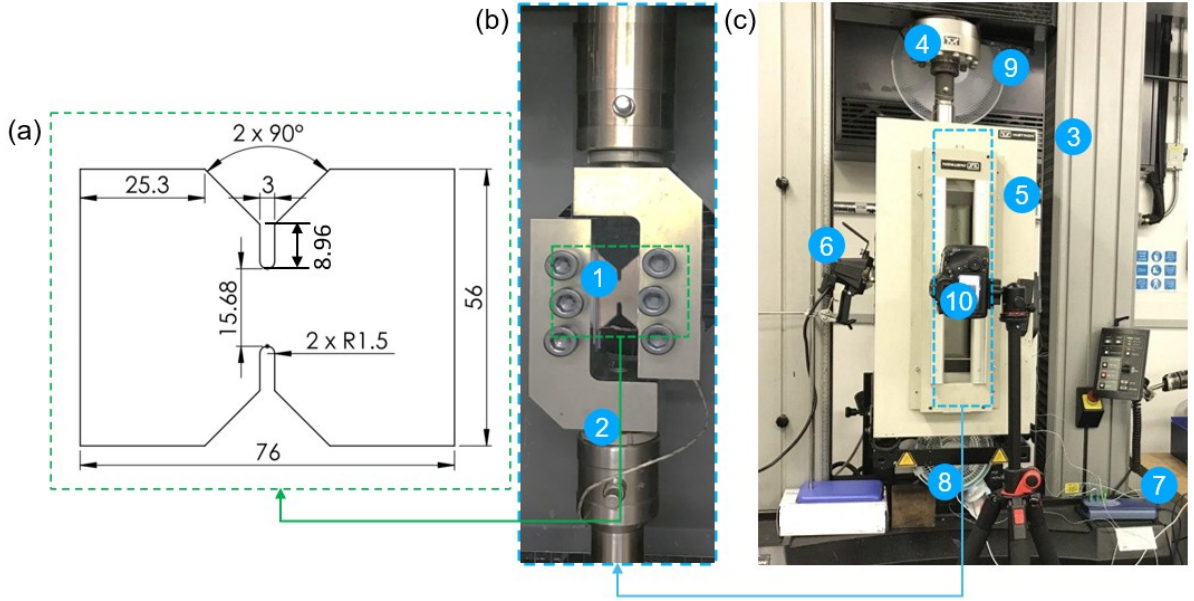


Fig. 1. Modified VNR specimen geometry and experimental setup. (a) Specimen dimensions (mm). (b) Specimen mounted in the VNR grips: (1) specimen and (2) grips. (c) Heated VNR test setup: (3) Instron 5584 load frame, (4) crosshead and load cell, (5) environmental chamber, (6) illumination, (7) thermocouples and data logger, (8, 9) cooling fans, and (10) camera.

2.2. Loading and DIC measurements

The tests were conducted using an INSTRON 5584 universal testing machine equipped with an environmental chamber. Thermocouples and a data logger monitored the specimen temperature. The test temperature was 180 °C and the crosshead speed was 1.57 mm/s. Three repeat tests were performed. Images were recorded at 60 frames/s under external illumination, and fans protected the load cell and surrounding equipment from overheating. The tensile reference curves were obtained from the earlier material-characterisation study [26]. The DIC measurement area and the ligament-line ROI used for strain extraction are shown in Fig. 2.

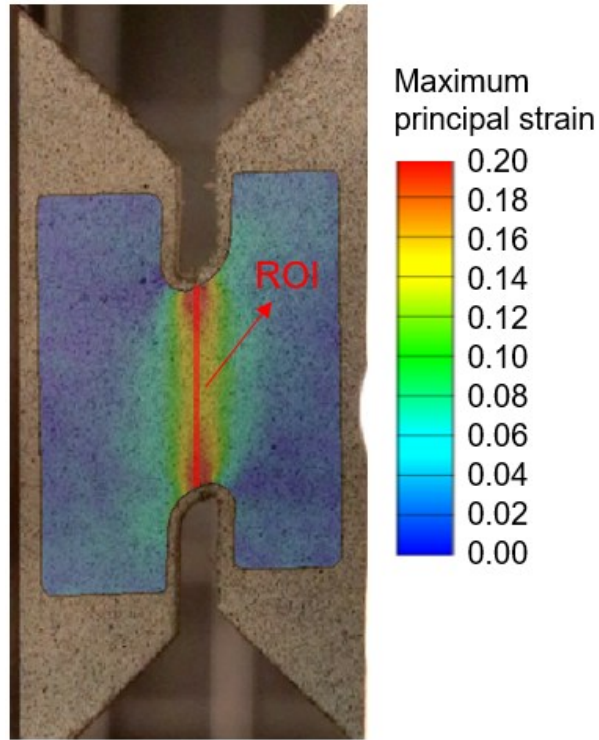


Fig. 2. Representative DIC maximum-principal-strain field on the VNR specimen, showing the ligament-line region of interest (ROI) used for strain extraction. The colour scale denotes the maximum principal engineering strain.

The images were processed using two-dimensional DIC software supplied by GOM, Germany. The reported areal pixel density was approximately 225 pixels/mm²; the facet size was 16 × 16 pixels and the facet spacing was 14 pixels. The ROI followed the ligament over its recognisable length and included the facets intersected by that line. The experimental strain used to calculate the stress–strain curve was the arithmetic mean of the maximum principal engineering strains of these facets. The subsequent numerical analysis compares calculations using this ROI average with calculations using strain at the ligament centre.

For DIC measurements in a heated chamber, image quality and the optical path through the heated air also affect the measurement, as reviewed by Yu and Pan [49]. Here, the measured strain fields show how deformation varies along the ligament and provide the ROI-averaged strain used to calculate the stress–strain curve.

2.3. Baseline calculation of the VNR stress–strain curve

The measured load and ROI-averaged DIC strain were first converted into an uncorrected VNR stress–strain curve using the baseline symmetric-shear reconstruction described below. This curve provides the baseline result for the subsequent error analysis and stress correction. Eq.

(1) gives the general definitions of equivalent logarithmic strain $\bar{\varepsilon}$ and von Mises equivalent stress $\bar{\sigma}$ from the ordered principal logarithmic strains and Cauchy stresses, respectively.

$$\begin{aligned}\bar{\varepsilon} &= \frac{\sqrt{2}}{3} \sqrt{(\varepsilon_1 - \varepsilon_2)^2 + (\varepsilon_2 - \varepsilon_3)^2 + (\varepsilon_3 - \varepsilon_1)^2}, \\ \bar{\sigma} &= \frac{\sqrt{2}}{2} \left(\sqrt{(\sigma_1 - \sigma_2)^2 + (\sigma_2 - \sigma_3)^2 + (\sigma_3 - \sigma_1)^2} \right).\end{aligned}\tag{1}$$

For the baseline VNR conversion, the measured quantities are the vertical load F (N), initial ligament cross-sectional area A_0 (mm²), and ROI-averaged maximum principal engineering strain $\bar{\varepsilon}_{ROI}^{eng}$. Under the baseline symmetric-shear idealisation, the load per initial ligament area is combined with the representative principal stretch, with incompressibility assumed, to reconstruct the corresponding principal Cauchy stress. With equal-and-opposite non-zero principal strains and stresses and a zero intermediate principal value, the baseline conversion becomes:

$$\begin{aligned}\varepsilon^t &= \ln(1 + \varepsilon_{ROI}^{eng}); \quad \bar{\varepsilon} = \frac{2}{\sqrt{3}} \varepsilon^t, \\ \sigma^t &= \frac{F}{A_0} (1 + \varepsilon_{ROI}^{eng}); \quad \bar{\sigma} = \sqrt{3} \sigma^t.\end{aligned}\tag{2}$$

Here, ε^t is the logarithmic strain corresponding to the averaged principal stretch and σ^t is the reconstructed principal Cauchy stress; the superscript *eng* denotes engineering strain. The intermediate principal-value substitutions leading to these expressions are given in Appendix A.1. The ligament-centre stress and strain used later as the numerical reference are instead obtained directly from the full numerical stress and strain tensors using Eq. (1), without the baseline principal-value assumption.

Using this baseline conversion, Fig. 3 compares the uncorrected VNR curve with the PEEK tensile reference at 180 °C. The difference increases with deformation, while the nominal shear rate of the VNR test (approximately 0.1 s⁻¹ from 1.57 mm/s over a 15.68 mm ligament) provides a nominal rate condition comparable to that of the 0.1 s⁻¹ tensile reference [26].

The tensile curve is used here as an external experimental reference rather than as the numerical correction target. The following sections therefore examine how the VNR stress calculation contributes to this increasing difference.

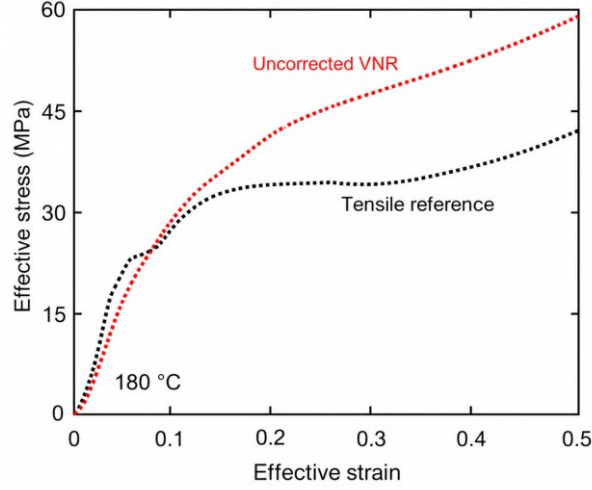


Fig. 3. Comparison of effective stress–strain responses for PEEK at 180 °C under nominally comparable rate conditions of approximately 0.1 s^{-1} . The red dotted curve is the uncorrected VNR response obtained using the baseline conversion in Section 2.3, and the black dotted curve is the tensile reference [26].

3. Sources of error in the VNR stress–strain calculation

The increasing stress error observed in the VNR calculation is examined in terms of two effects: the change in principal directions during large simple shear and the non-uniform deformation along the ligament. Section 3.1 examines how the principal directions evolve in simple shear and why this affects the baseline load-to-stress conversion. Section 3.2 then considers how non-uniform deformation leads to differences between local and ROI-averaged strain measures.

3.1. Principal-direction change in simple shear and its effect on VNR stress calculation

Fig. 4 illustrates the difference between the local simple-shear state and the small-deformation idealisation used in the baseline VNR stress conversion. In Fig. 4a, the measured vertical load is the resultant force transmitted across the ligament, while the local material element is described by the normal stress components σ_{11} and σ_{22} and the shear component σ_{12} . The angle $\gamma_{m,s}$ describes the simple-shear deformation. Fig. 4b shows the corresponding symmetric-shear construction used in the conventional small-deformation idealisation, with shear angle $\gamma_{m,p}$. The subscripts s and p distinguish the simple-shear and symmetric-shear constructions, respectively, and both angles are expressed in radians. This idealisation provides a simple basis for converting the measured load into an equivalent stress, but its fixed-direction assumption becomes less representative as deformation increases.

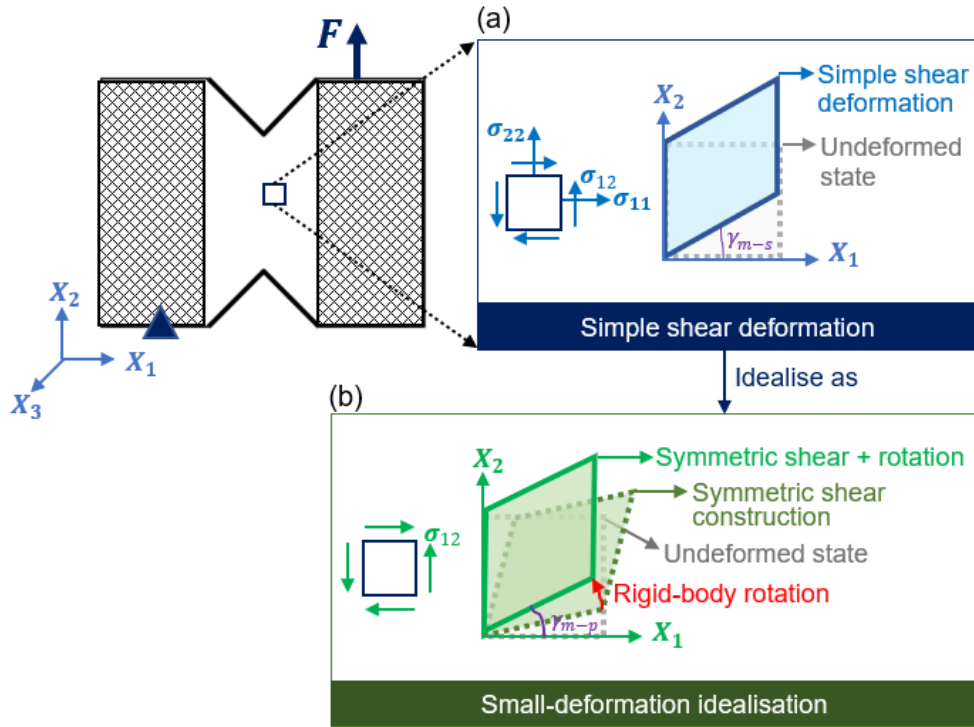


Fig. 4. Kinematic and stress idealisations used to interpret VNR loading. (a) Local simple-shear deformation in the ligament, showing the measured resultant load F , stress components and shear angle $\gamma_{m,s}$. (b) Symmetric-shear construction used in the conventional small-deformation idealisation, with shear angle $\gamma_{m,p}$ and the associated rigid-body rotation.

At large deformation, the local simple-shear deformation no longer follows the fixed principal directions assumed in the baseline conversion. Fig. 5 examines the kinematic origin of this directional change. Panel (a) shows the symmetric-shear construction used as the reference, whereas panel (b) shows the simple-shear deformation represented by the right polar decomposition $\mathbf{F}_s = \mathbf{R}\mathbf{U}$. Here, $\mathbf{U}$ describes the stretch associated with simple shear and $\mathbf{R}$ describes the rigid-body rotation. Together, they represent the same simple-shear deformation.

In Fig. 5, the superscripts o , U and d denote the original, stretched and deformed configurations, respectively. The detailed coordinate mappings and angle definitions are given in Appendix A.2.

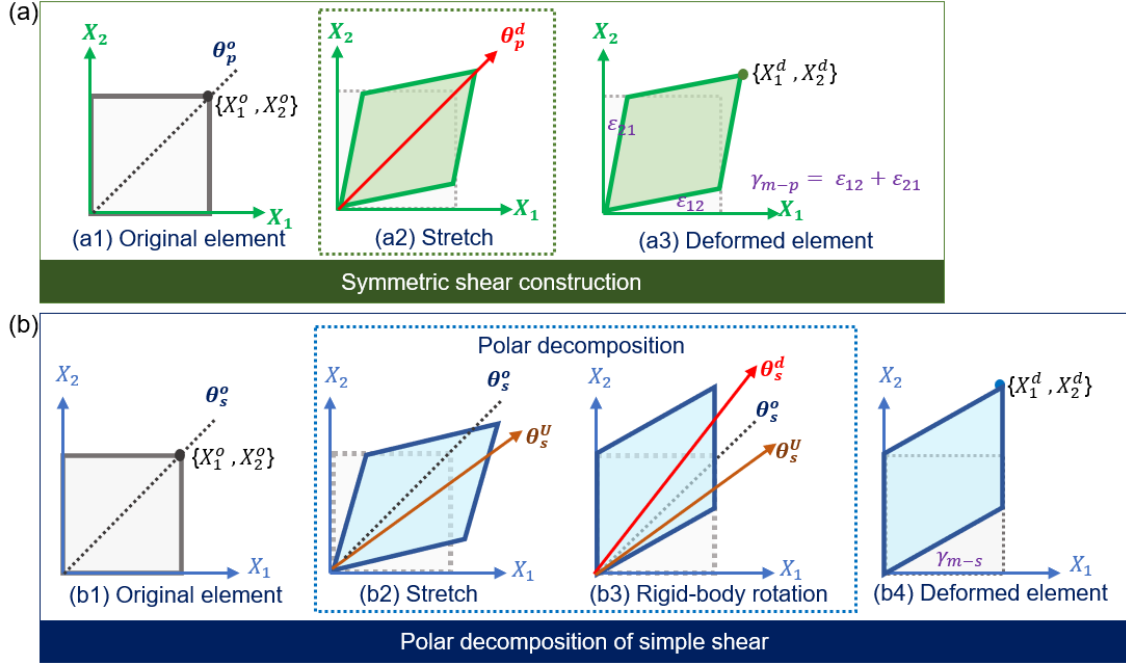


Fig. 5. Kinematic constructions for (a) symmetric shear and (b) simple shear decomposed into stretch and rotation. The angles θ_p^o and θ_p^d describe the maximum-principal directions in the original and deformed symmetric construction; θ_s^o , θ_s^U and θ_s^d describe the corresponding simple-shear directions. The changes θ_s^{oU} , θ_s^{Ud} and θ_s^{od} denote the stretch-related reorientation, rigid-body rotation and total principal-direction change, respectively. Superscripts o , U and d denote the original, stretched and deformed configurations. All angles are expressed in radians.

For the symmetric-shear construction in Fig. 5a, the deformation gradient is symmetric and the in-plane principal directions remain fixed at $\pm 45^\circ$ to the coordinate axes. This fixed-direction state provides the reference for the conventional small-deformation conversion. The coordinate mapping and incompressibility steps leading to this result are given in Appendix A.2.

For simple shear, the coordinate mapping is different. The full mapping is given in Appendix A.2, and differentiating it gives the deformation gradient in Eq. (3).

$$\mathbf{F}_s = \frac{\partial \mathbf{X}^d}{\partial \mathbf{X}^o} = \begin{bmatrix} 1 & 0 & 0 \\ \tan(\gamma_{m,s}) & 1 & 0 \\ 0 & 0 & 1 \end{bmatrix}. \quad (3)$$

Eq. (3) describes simple shear in which X_1 remains unchanged, while the X_2 coordinate acquires an additional displacement proportional to X_1 . The shear term $\tan(\gamma_{m,s})$ therefore increases with the imposed simple-shear deformation.

Applying the right polar decomposition $\mathbf{F}_s = \mathbf{R}\mathbf{U}$ to the simple-shear deformation gradient and defining $b = \frac{\tan(\gamma_{m,s})}{2}$ gives the rotation and stretch tensors

$$\mathbf{R} = \begin{bmatrix} 1 & -b & 0 \\ \frac{1}{\sqrt{1+b^2}} & \frac{b}{\sqrt{1+b^2}} & 0 \\ \frac{b}{\sqrt{1+b^2}} & \frac{1}{\sqrt{1+b^2}} & 0 \\ 0 & 0 & 1 \end{bmatrix}; \quad \mathbf{U} = \begin{bmatrix} 1+2b^2 & b & 0 \\ \frac{1}{\sqrt{1+b^2}} & \frac{b}{\sqrt{1+b^2}} & 0 \\ \frac{b}{\sqrt{1+b^2}} & \frac{1}{\sqrt{1+b^2}} & 0 \\ 0 & 0 & 1 \end{bmatrix}. \quad (4)$$

Eq. (4) shows how the same simple-shear deformation is represented by the two parts of the polar decomposition. The scalar b introduced above is a compact measure of the simple-shear angle used to write the matrix coefficients. The principal directions of $\mathbf{U}$ depend on the shear amount b and therefore depart progressively from the initial 45° reference at finite shear. In the polar-decomposition representation, $\mathbf{R}$ then rotates the stretched configuration further. Therefore both parts contribute to the final change in principal direction, even though rigid-body rotation itself does not change the von Mises stress.

The corresponding principal directions are defined in Fig. 5. The intermediate eigenvector and angle transformations are given in Appendix A.2; combining the reorientation associated with the stretch tensor and the additional rigid-body rotation gives the total principal-direction change in Eq. (5).

$$\theta_s^{\text{od}} = \theta_s^{\text{ou}} + \theta_s^{\text{ud}} = \arctan\left(\frac{b}{\sqrt{1+b^2} + 1}\right). \quad (5)$$

Eq. (5) is the key kinematic result of this section. It expresses the total change in the selected principal direction as the combined effect of the stretch-related reorientation and rigid-body rotation. This total principal-direction change is used to characterise the first source of stress-calculation error. The error arises from the mismatch between the evolving principal directions in simple shear and the fixed-direction symmetric-shear assumption used in the baseline VNR conversion. The second source of error, associated with non-uniform deformation and the difference between local and ROI-averaged strain measures, is considered in Section 3.2.

3.2. Non-uniform ligament deformation and representative strain measures

The second effect considered in this study arises from the spatial non-uniformity of deformation along the VNR ligament. The measured load is a resultant transmitted across the ligament, whereas the local stress and strain vary with position. A stress–strain curve constructed from this resultant load therefore depends on how the representative strain is defined. When deformation is non-uniform, a local ligament-centre measure and a spatially averaged measure need not represent the same part of the ligament response.

To examine this effect, two representative strain-extraction routes are considered in Fig. 6. The first is a local strain-gauge-type (SG-type) measure at the ligament centre, used as a numerical analogue of the local sampling provided by a conventional strain-gauge measurement. The second is an ROI-averaged measure corresponding to the DIC procedure used in the experiments. Together, the two routes provide a direct comparison between a local centre-point strain measure and a spatially averaged strain measure over the DIC ROI.

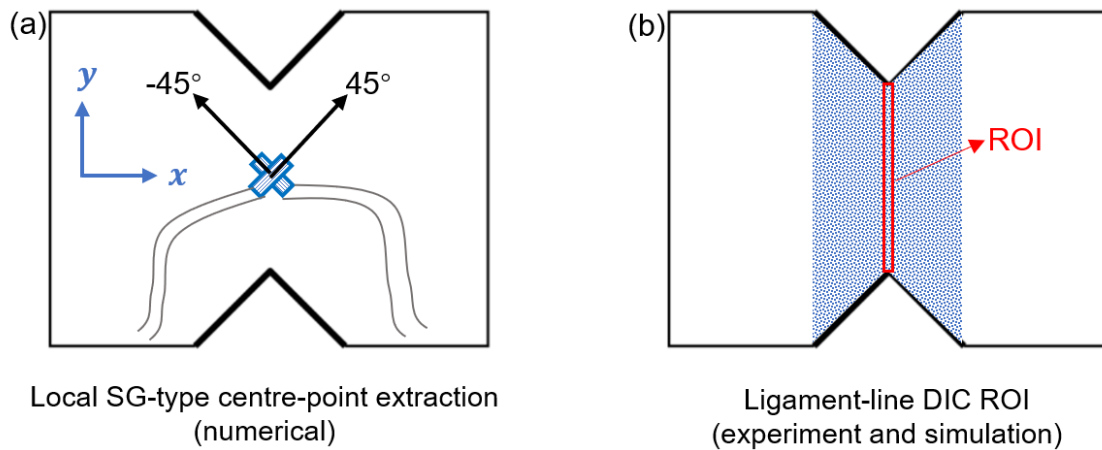


Fig. 6. Representative strain-extraction routes used in the VNR analysis. (a) Local SG-type extraction at the ligament centre, illustrated using a conventional strain-gauge arrangement and evaluated numerically. (b) Ligament-line DIC region of interest (ROI) used for the experimental and numerical strain extraction.

Fig. 6a illustrates the conventional local strain-gauge arrangement. In the numerical SG-type route used here, the representative strain is extracted directly from the ligament-centre strain state, consistent with the centre-point calculation introduced in Section 5.2. The conventional $\pm 45^\circ$ strain-gauge transformation is provided in Appendix A.3 for reference.

The DIC route uses the spatial strain field rather than a single material point. As described in Section 2.2, the maximum principal engineering strain is obtained at the DIC facets intersected by the ligament-line ROI and averaged to provide the representative experimental strain used in the VNR stress–strain curve. Previous shear studies have adopted different spatial definitions, including small central regions, multiple ligament subregions, and circular or rectangular central regions [8,10,11,20]. This variation illustrates that, once the strain field is non-uniform, the representative strain obtained from DIC depends on the spatial region over which it is evaluated.

If the ligament deformation were spatially uniform, the local ligament-centre strain and the ROI-averaged strain would provide essentially the same representation of the deformation. In the VNR specimen, however, the strain varies along the ligament, as indicated by the DIC field

in Fig. 2. The local SG-type measure represents the deformation at the ligament centre, whereas the ROI-averaged DIC measure represents the average deformation over the selected ligament region.

When either strain measure is combined with the same resultant load to construct a VNR stress–strain curve, this difference in strain representation is carried into the calculated response. The finite element model introduced in Section 4 is therefore used to compare these load-derived responses with numerical stresses obtained over the ROI and at the ligament centre.

4. Finite element model and experimental assessment

Finite element analysis is used to provide the tensor-based stress and strain quantities required for the comparisons introduced in Section 3. It also provides a prescribed material response against which the stress–strain curves calculated from the simulated VNR test can be assessed. This section first defines the material model and VNR simulation, and then compares the predicted load response and ligament strain distribution with the experiments. The assessed model is subsequently used in Section 5 to establish the numerical references required for the error analysis and stress correction.

4.1. Temperature-dependent material model and calibration

A temperature-dependent Johnson–Cook (JC) model was used to define the PEEK material response in the finite element simulations. The model combines linear elasticity with a temperature-dependent plastic flow law calibrated using the tensile responses at 180, 240 and 300 °C reported in [26]. The calibrated model defines the prescribed material response used in the VNR simulation, against which the stress–strain curves calculated from the simulated VNR test can subsequently be compared.

The plastic flow stress is expressed as

$$\sigma = \left[A + B(\bar{\epsilon}^{pl})^n \right] \left(1 - \left(\frac{T - T_0}{T_m - T_0} \right)^m \right); \quad T_0 \leq T < T_m, \quad (6)$$

where σ is the flow stress (MPa), $\bar{\epsilon}^{pl}$ is the dimensionless accumulated equivalent plastic strain, and T , T_0 and T_m are the current, reference and melting temperatures (K), respectively. A (MPa) defines the reference yield-stress level, B (MPa) and n (dimensionless) control the magnitude and curvature of strain hardening, and m (dimensionless) controls thermal softening.

At the reference temperature T_0 , the thermal term in Eq. (6) is equal to unity, so the reference-temperature flow curve is governed by A , B and n . The tensile responses above T_0 provide the temperature dependence required to determine the thermal-softening parameter m .

The constitutive strain variable $\bar{\epsilon}^{pl}$ in Eq. (6) is the accumulated equivalent plastic strain used to describe plastic hardening. The stress–strain comparisons elsewhere in this study use the total equivalent logarithmic strain defined in Eq. (1). For the monotonic uniaxial tensile data used in the calibration, the post-yield plastic strain was approximated as $\bar{\epsilon}^{pl} \approx \ln(\lambda) - \frac{\sigma}{E}$, where λ is the dimensionless axial stretch, defined as the ratio of the current to initial length, and E is Young’s modulus (MPa). This approximation assumes that the elastic strain is small, such that $\frac{\sigma}{E}$ provides an adequate estimate of the elastic contribution to the logarithmic strain. The resulting flow stress–plastic strain data were used to calibrate Eq. (6).

The yield properties were determined following the procedure reported in [26], and the constitutive parameters were fitted by nonlinear least squares using the MATLAB function `lsqnonlin`. The resulting JC responses are compared with the tensile data in Fig. 7. These calibrated curves define the material input used in the VNR simulations described in Section 4.2.

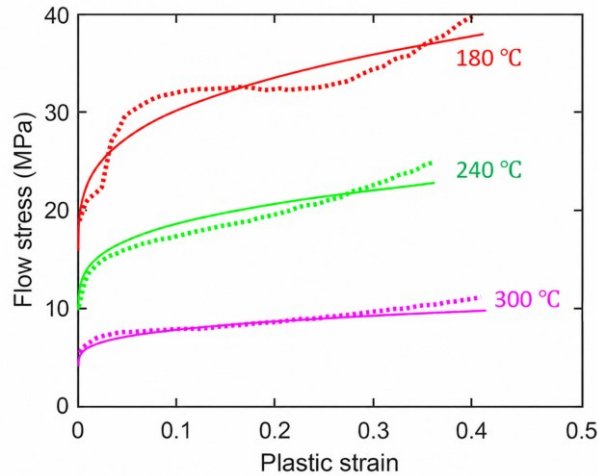


Fig. 7. Temperature-dependent Johnson–Cook (JC) fits to the PEEK tensile flow stress–plastic strain responses at 180, 240 and 300 °C and a strain rate of 0.1 s^{-1} . Solid lines denote the JC model fits, and dotted lines denote the experimental tensile data from [26].

4.2. Finite element model setup

The modified VNR specimen geometry described in Section 2.1 was reproduced in ABAQUS. The model consisted of 6283 four-node reduced-integration shell elements (S4R), with five integration points through the thickness. The boundary conditions and displacement loading

were defined to reproduce the experimental gripping and loading configuration. A total displacement of 5 mm was applied, and the analysis was performed using ABAQUS/Explicit. The temperature-dependent material model calibrated in Section 4.1 was assigned to the specimen. The elastic properties, JC parameters and predefined specimen temperature used in the simulation are summarised in Table 1. The analysis temperature was 453 K (180 °C), corresponding to the experimental VNR test condition.

Table 1. Input parameters for the VNR shear simulation.

Properties	Parameters
Density (tonne/mm ³)	$\rho = 1.3 \times 10^{-9}$
Modulus (MPa)	$E = 415.948$
Poisson's ratio	$\nu = 0.48$
JC model parameters (A, B, n, m, T_0, T_m)	$A = 15.651,$ $B = 29.079,$ $n = 0.298,$ $m = 0.947,$ $T_0 = 453 \text{ K (180 °C)}$ $T_m = 616 \text{ K (343 °C)}$
Predefined field temperature	453 K (180 °C)

4.3. Comparison with experimental load and strain fields

The finite element model was assessed against the experimental response before being used for the error analysis in Section 5. Two aspects were compared: the global load–displacement response and the spatial distribution of strain along the VNR ligament. The former evaluates the overall mechanical response of the specimen, while the latter is particularly important because the non-uniform ligament deformation identified in Section 3.2 is central to the subsequent comparison of local and ROI-averaged strain measures.

Fig. 8a compares the simulated and DIC-measured maximum principal strain profiles along the ligament at four displacement stages. Position along the ligament is normalised from 0 at the lower end to 1 at the upper end. Both the experimental and numerical profiles show a non-uniform distribution, with higher strains developing towards the ligament ends and lower strains over the central region. The finite element model reproduces the overall shape and evolution of these profiles over the four deformation stages.

The full-field comparison at Stage 3, corresponding to a displacement of approximately 1.98 mm, is shown in Fig. 8b and c. Fig. 8b presents the maximum principal strain field measured by DIC, whereas Fig. 8c shows the corresponding field predicted by the finite element model.

Both exhibit the same main deformation pattern, including strain concentrations near the upper and lower notch regions and a lower-strain region through the ligament centre.

The global response is compared in Fig. 8d. The simulation captures the overall development of the experimental load–displacement curve, although some differences remain over the deformation history. Taken together, the load response and ligament strain comparisons show that the model reproduces the principal global and local features required for the subsequent error analysis. The assessed model is therefore used in Section 5 to select a suitable local reference and to obtain the ROI-averaged and ligament-centre stress and strain quantities required for the error analysis and correction.

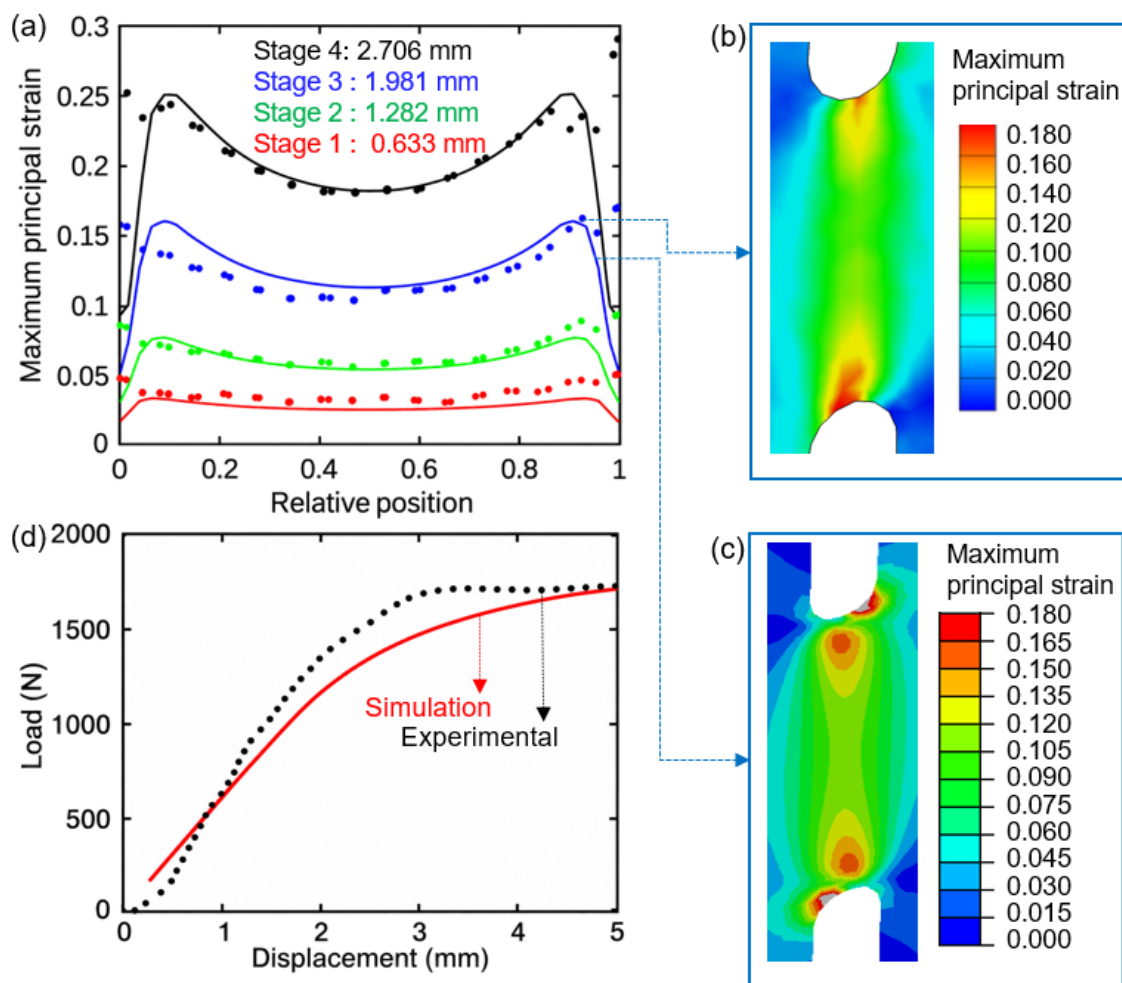


Fig. 8. Experimental and numerical assessment of the VNR finite element model. (a) Maximum-principal-strain profiles along the normalised ligament position at four displacement stages; solid lines denote simulations and symbols denote DIC measurements. (b) DIC-measured and (c) finite-element-predicted maximum principal strain fields at Stage 3 (1.981 mm displacement). (d) Experimental and numerical load–displacement responses. The ligament position in (a) is normalised from 0 at the lower end to 1 at the upper end.

5. Quantification and correction of the VNR stress-calculation error

The finite element model established and assessed in Section 4 is used here to quantify and correct the VNR stress-calculation error. A ligament-centre numerical reference is first established, after which the local SG-type and ROI-based DIC calculations are compared with this reference. The stress correction is then applied in two successive steps using an ROI-averaged numerical response as the intermediate reference and the ligament-centre response as the final reference. The corrected response is subsequently assessed against the prescribed numerical material response and the experimental tensile reference, followed by evaluation of its effect on constitutive identification and deformation prediction. Additional numerical cases are finally considered for PEEK at another temperature and for PA6.

5.1. Selection of the local reference location

A local numerical reference is required to assess the stress and strain calculated from the measured resultant load. Two candidate locations were therefore examined in the finite element model, as shown in Fig. 9a: Node 1 lies within the upper strain-concentration region, whereas Node 2 is located at the ligament centre.

For simple shear, the two in-plane principal stretches have a product of unity, giving equal-and-opposite principal logarithmic strains. The relation $\varepsilon_{max} = -\varepsilon_{min}$ therefore provides a kinematic indication of how closely the local deformation approaches the simple-shear state.

Fig. 9b compares the maximum and minimum principal strains at the two locations over the analysed deformation range. The green line represents the equal-and-opposite condition. The ligament-centre response at Node 2 remains close to this relation, whereas Node 1 increasingly departs from it as deformation increases. Node 2 at the ligament centre is therefore selected as the location for the local numerical reference. Its stress-strain response is assessed against the prescribed material response in Section 5.2.

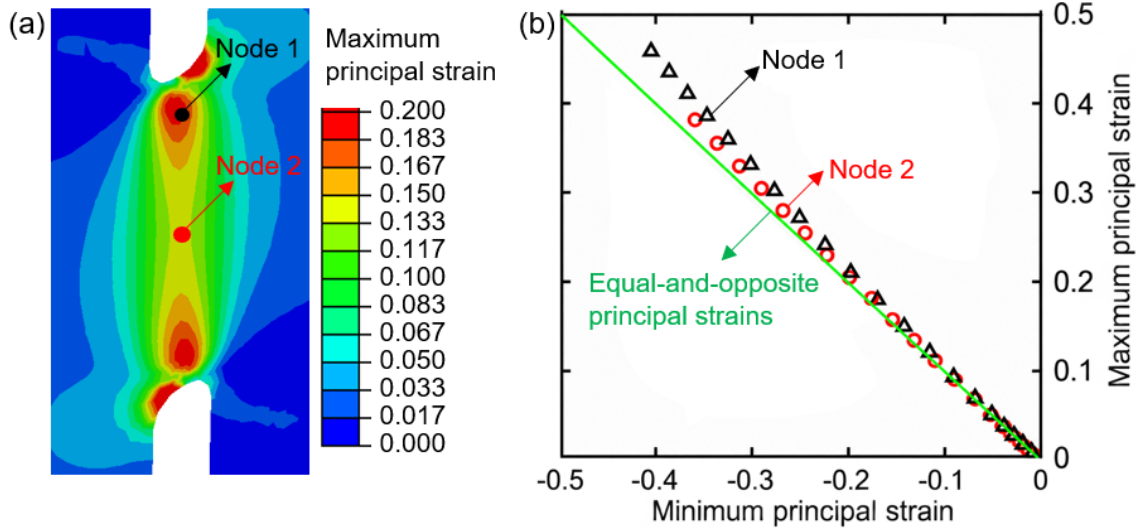


Fig. 9. Selection of the ligament-centre location for the local numerical reference. (a) Maximum-principal-strain field showing Node 1 in the upper strain-concentration region and Node 2 at the ligament centre. (b) Maximum versus minimum principal logarithmic strains at the two locations over the analysed deformation range; black triangles denote Node 1 and red circles denote Node 2. The green line represents the equal-and-opposite principal-strain condition associated with simple shear.

5.2. Comparison of calculated VNR responses with the ligament-centre reference

Following the selection of the ligament centre in Section 5.1, its local stress–strain response is first assessed against the prescribed material response. The principal stress and logarithmic-strain components are extracted directly from the finite element model, and Eq. (1) gives the centre-point effective strain ε_c and equivalent stress σ_c , respectively. The subscript c denotes the ligament-centre numerical reference.

Fig. 10a compares the ligament-centre finite element response with the prescribed JC material response used in the simulation. Because ε_c is a total logarithmic strain, the JC input response is plotted on the same total-strain basis by including the corresponding elastic contribution in addition to the accumulated plastic strain defined in Section 4.1. The close agreement in Fig. 10a supports the use of the ligament-centre response as the local numerical reference for the following comparisons.

With the local numerical reference established, the finite element results are next processed using the two representative strain-extraction routes introduced in Section 3.2. In both cases, the VNR stress–strain response is calculated from the resultant load and the corresponding strain measure, rather than obtained directly from the local finite element stress tensor.

For the numerical SG-type route, the representative strain is taken at the ligament centre. Because Node 2 remains close to the equal-and-opposite principal-strain condition identified in Section 5.1, the centre-point logarithmic principal strains are represented as h_c , 0 and $-h_c$, where h_c denotes the maximum principal logarithmic strain. Substitution into Eq. (1), together with the baseline load-to-stress conversion introduced in Section 2.3, gives the SG-type response:

$$\begin{aligned}\varepsilon_{SG} &= \frac{2}{\sqrt{3}} h_c, \\ \sigma_{SG} &= \sqrt{3} \frac{F}{A_0} \exp\left(\frac{\sqrt{3}\varepsilon_{SG}}{2}\right),\end{aligned}\tag{7}$$

where A_0 is the initial ligament cross-sectional area. The two relations in Eq. (7) give the SG-type effective strain and corresponding load-derived stress, respectively.

For the ROI-based DIC calculation, the facet-wise maximum principal engineering strains are first averaged over the ligament-line ROI. Let $e_{1,i}$ denote the maximum principal engineering strain of facet i , and N the number of included facets. The ROI average is then converted to logarithmic strain and, using the same equal-and-opposite principal-strain representation (h_{DIC} , 0, $-h_{DIC}$), to effective strain and stress:

$$\begin{aligned}\bar{e}_{ROI} &= \varepsilon_{ROI}^{eng} = \frac{1}{N} \sum_{i=1}^N e_{1,i}, \\ h_{DIC} &= \ln(1 + \bar{e}_{ROI}), \\ \varepsilon_{DIC} &= \frac{2}{\sqrt{3}} h_{DIC} = \frac{2}{\sqrt{3}} \ln(1 + \bar{e}_{ROI}), \\ \sigma_{DIC} &= \sqrt{3} \frac{F}{A_0} \exp\left(\frac{\sqrt{3}\varepsilon_{DIC}}{2}\right).\end{aligned}\tag{8}$$

The four relations in Eq. (8) give the ROI-averaged engineering strain, its logarithmic conversion, the effective strain and the corresponding load-derived DIC stress, respectively. The latter two use the same equivalent-strain definition in Eq. (1) and baseline load-to-stress conversion as the SG-type route.

The SG-type and DIC routes use different representative strains but the same resultant load and baseline conversion. The resulting stresses, σ_{SG} and σ_{DIC} , are subsequently compared with the ligament-centre reference stress σ_c , which is obtained directly from the local finite element stress tensor using Eq. (1).

Fig. 10b and c compare the two calculated VNR responses with the ligament-centre numerical reference. As shown in Fig. 10b, both ε_{SG} and ε_{DIC} remain comparatively close to the centre-point strain ε_c over the analysed deformation range, although differences develop between the local and ROI-averaged strain measures. In contrast, Fig. 10c shows that both calculated

stresses increasingly exceed the centre-point stress σ_c as deformation increases. At the displacement corresponding to $\varepsilon_c = 0.10$, the relative stress errors of both calculations are about 12%. For calculation route r , where $r \in \{SG, DIC\}$, the relative stress error is defined as

$$\xi_r = \frac{\sigma_r - \sigma_c}{\sigma_c} \times 100\%. \quad (9)$$

The comparison therefore shows that the deformation-dependent discrepancy is concentrated in the stress calculation. Both representative strain measures remain comparatively close to the ligament-centre strain, whereas the corresponding load-derived stresses increasingly depart from the tensor-derived centre stress. The correction developed in Section 5.3 therefore acts on the calculated stress, using an ROI-averaged numerical response as an intermediate reference before comparison with the ligament-centre response.

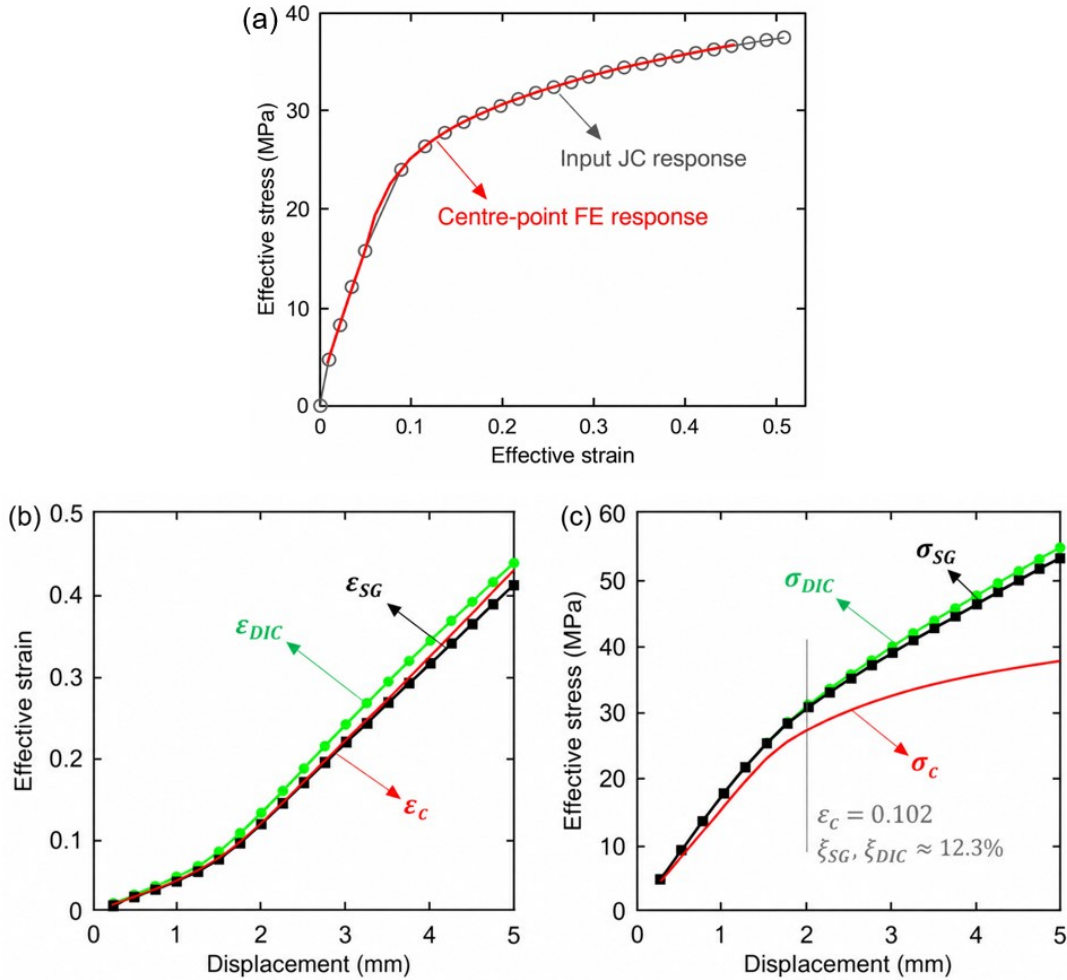


Fig. 10. Ligament-centre numerical reference and comparison of calculated VNR responses. (a) Ligament-centre FE stress–strain response compared with the prescribed JC material response. (b) Effective strain and (c) equivalent stress from the local SG-type and ROI-based DIC calculations, compared with the ligament-centre reference as functions of displacement. The subscript c denotes the ligament-centre reference; SG and DIC denote the local SG-type and ROI-based DIC calculations, respectively. In (c), both calculated stresses show an error of about 12% at $\varepsilon_c = 0.10$.

5.3. Two-step correction of the calculated VNR stress

Section 5.2 showed that the increasing discrepancy in the calculated VNR response is concentrated in the stress calculation. The correction is therefore applied through two successive numerical references. Step 1 compares the load-derived stress with an ROI-averaged stress obtained directly from the finite element stress field. This step targets the contribution associated with principal-direction change in the load-to-stress conversion. Step 2 then compares the first corrected stress with the ligament-centre response. This step targets the remaining difference associated with non-uniform ligament deformation. The same procedure is applied to the local SG-type and ROI-based DIC calculations.

Step 1 uses the same ligament-line ROI as the DIC analysis. At each ROI point i , $e_{1,i}$ and $\sigma_{1,i}$ denote the maximum principal engineering strain and maximum principal Cauchy stress (MPa), respectively. The ROI-averaged logarithmic strain and maximum principal stress are defined as $\bar{h}_{ROI} = N^{-1} \sum_{i=1}^N \ln(1 + e_{1,i})$ and $\bar{\sigma}_{ROI} = N^{-1} \sum_{i=1}^N \sigma_{1,i}$. Using the same equal-and-opposite representation as in Section 5.2 gives $\varepsilon_{ROI} = \frac{2}{\sqrt{3}} \bar{h}_{ROI}$ and $\sigma_{ROI} = \sqrt{3} \bar{\sigma}_{ROI}$. Unlike h_{DIC} in Eq. (8), which is obtained after averaging the engineering strains, the ROI reference averages the local logarithmic strains before the effective-strain conversion. The resulting σ_{ROI} provides the intermediate numerical reference for Step 1.

For a representative maximum principal logarithmic strain h , the corresponding polar rotation angle $\omega(h)$, expressed in radians, is

$$\omega(h) = \arctan[\sinh(h)], \quad (10)$$

as obtained from the simple-shear kinematics in Section 3.1. Accordingly, $\omega_{SG} = \omega(h_c)$, $\omega_{DIC} = \omega(h_{DIC})$ and $\omega_{ROI} = \omega(\bar{h}_{ROI})$ are evaluated from the corresponding strain measures, and the numerical stress ratios are represented as functions of ω , which provides a common deformation measure for the two calculation routes.

Step 1 is shown in Fig. 11a. The calculated stresses are first compared with σ_{ROI} . Based on the numerical comparison, the corresponding stress ratios are represented by the exponential relation $f_r^1 = \exp(\omega_r/2)$. Dividing by this factor gives the first corrected stress, which approaches the ROI reference.

Step 2 is shown in Fig. 11b. The first corrected stress is then compared with the ligament-centre stress σ_c . Based on the numerical comparison, the remaining stress ratio is represented by $f_r^2 = \exp(2\omega_r/3)$, giving the final corrected stress. The superscripts *corr1* and *corr2* denote

stresses after the first and second correction steps, respectively; f_{r1} and f_{r2} are dimensionless correction factors. Table 2 summarises the two correction steps.

Table 2. Summary of the two-step stress correction for $r \in \{SG, DIC\}$. Rotation angles are in radians.

Step	Numerical reference and stress ratio	Correction factor	Corrected stress
Principal-direction change	$\sigma_{ROI}; \sigma_r/\sigma_{ROI}$	$f_r^1 = \exp(\omega_r/2)$	$\sigma_r^{corr1} = \sigma_r/f_r^1$
Non-uniform ligament deformation	$\sigma_c; \sigma_r^{corr1}/\sigma_c$	$f_r^2 = \exp(2\omega_r/3)$	$\sigma_r^{corr2} = \sigma_r^{corr1}/f_r^2$

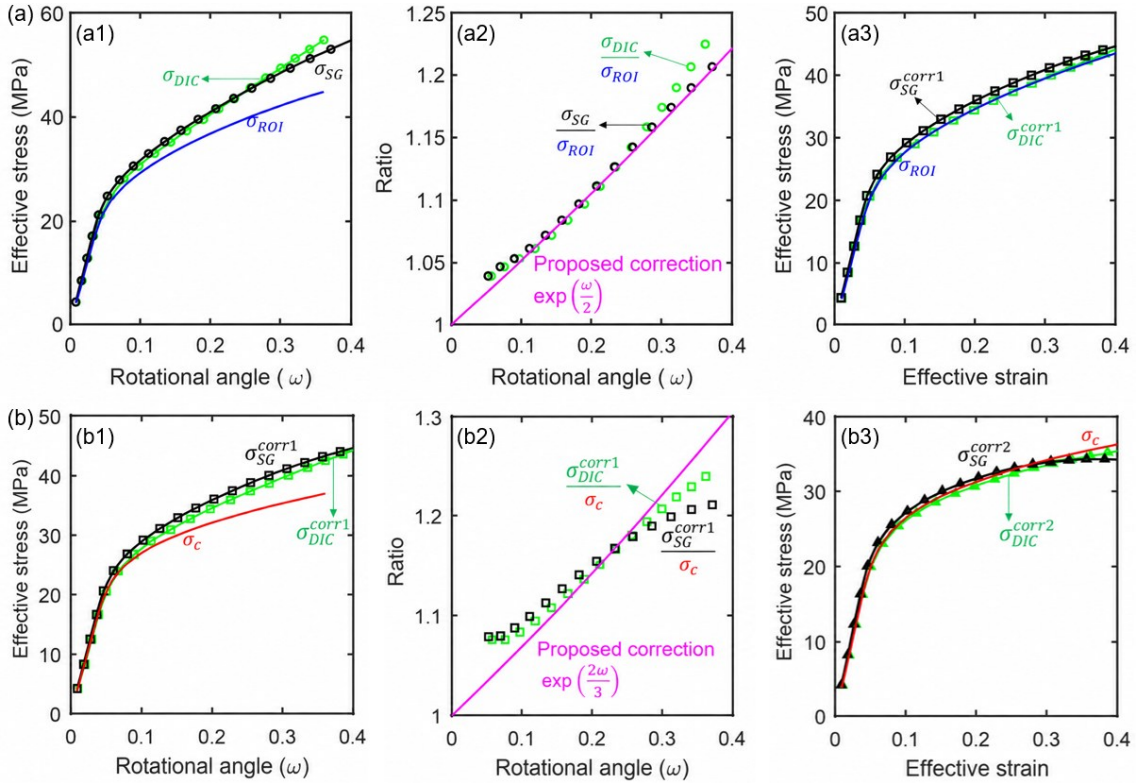


Fig. 11. Two-step correction of the calculated VNR stress. (a) Step 1 targets the contribution associated with principal-direction change using the ROI-averaged numerical response as the intermediate reference: (a1) stress comparison, (a2) stress ratio and correction factor, and (a3) first-corrected stresses. (b) Step 2 targets the remaining difference associated with non-uniform ligament deformation using the ligament-centre response: (b1) stress comparison, (b2) stress ratio and correction factor, and (b3) final corrected stresses. Superscripts *corr1* and *corr2* denote the stresses after the first and second correction steps, respectively; σ_{SG} , σ_{DIC} , σ_{ROI} , and σ_c denote the local SG-type calculation, ROI-based DIC calculation, ROI-averaged reference, and ligament-centre reference, respectively.

Both correction factors approach unity as $\omega_r \rightarrow 0$, so the correction recovers the small-deformation limit. Combining the two steps gives

$$\sigma_r^{corr2} = \sigma_r \exp\left(-\frac{7\omega_r}{6}\right), \quad r \in \{SG, DIC\}. \quad (11)$$

Fig. 12a compares the numerically calculated stress–strain curves before and after the two-step correction with the prescribed JC material response. Before correction, both the local SG-type

and ROI-based DIC calculations increasingly overestimate the input response as deformation grows, whereas after correction both curves follow the prescribed response closely over the analysed deformation range.

Once established from the numerical comparisons, the same correction was applied to the experimental PEEK VNR response at 180 °C using the measured load and selected strain measure to evaluate σ_r and the corresponding ω_r . As shown in Fig. 12b, the corrected VNR curve is substantially closer to the tensile reference than the uncorrected curve in Fig. 3, particularly at larger deformation. The numerical comparison in Fig. 12a establishes the correction against the prescribed material response, while Fig. 12b provides an experimental check against the tensile data.

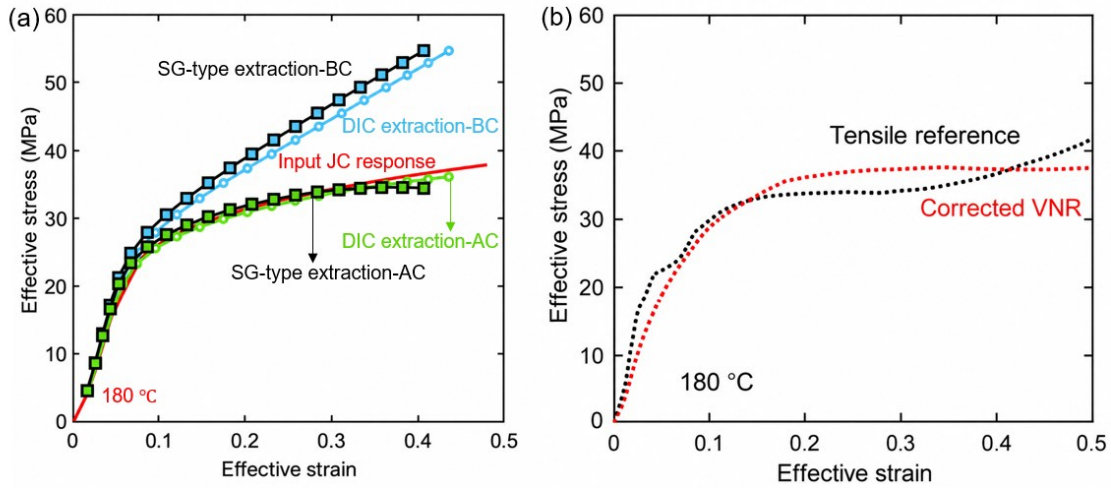


Fig. 12. Stress–strain responses before and after correction. (a) Numerically calculated effective stress–strain curves before (BC) and after (AC) correction, compared with the input JC response; SG and DIC denote the local SG-type and ROI-based DIC calculations, respectively. BC and AC denote before and after correction, respectively. (b) Experimental PEEK tensile reference [26] and corrected VNR response at 180 °C. The uncorrected VNR response is shown in Fig. 3.

5.4. Effect of stress correction on constitutive identification and predicted strain fields

Section 5.3 established the corrected VNR response. This section examines how using the corrected rather than uncorrected curve changes the identified material parameters and the deformation predicted using those parameters.

The uncorrected and corrected experimental VNR curves at 180 °C were separately converted to flow stress–plastic strain form and used to identify the elastic modulus and the strain-hardening parameters A , B and n . As these data were obtained at the reference temperature T_0 ,

the thermal-softening parameter m is inactive under this condition and was therefore fixed to the value obtained from the multi-temperature tensile calibration in Section 4.1.

Fig. 13a shows that the uncorrected VNR response produces a substantially stronger fitted hardening response than the corrected curve, and the corresponding parameter sets are listed in Table 3. In particular, the fitted hardening exponent n decreases from 0.530 to 0.245 after correction, together with a marked reduction in B . Because the stress overestimate increases with deformation, fitting the uncorrected curve incorporates part of the calculation error into the apparent strain-hardening response.

Table 3. Parameter sets used for simulations based on the uncorrected and corrected VNR responses.

Properties	Parameters before correction	Parameters after correction
Density (tonne/mm ³)	$\rho = 1.3 \times 10^{-9}$	$\rho = 1.3 \times 10^{-9}$
Modulus (MPa)	$E = 372.314$	$E = 312.559$
Poisson's ratio	$\nu = 0.48$	$\nu = 0.48$
JC model parameters (A, B, n, m, T_0, T_m)	$A = 18.517,$ $B = 62.864,$ $n = 0.530,$ $m = 0.947,$ $T_0 = 453 \text{ K (180 °C)}$ $T_m = 616 \text{ K (343 °C)}$	$A = 14.032,$ $B = 29.569,$ $n = 0.245,$ $m = 0.947,$ $T_0 = 453 \text{ K (180 °C)}$ $T_m = 616 \text{ K (343 °C)}$
Predefined field temperature	453 K (180 °C)	453 K (180 °C)

To assess whether the parameter differences in Table 3 affect subsequent model prediction, the two parameter sets were separately assigned to the same VNR finite element model and the predicted ligament strain profiles were compared with the DIC measurements in Fig. 13b and c. At the earliest displacement stage, both calibrations give similar profiles. As deformation increases, the simulation using parameters fitted to the uncorrected VNR curve increasingly departs from the DIC measurements, whereas the corrected-data calibration reproduces the measured strain distribution more closely. The calculation error is therefore carried into both the fitted hardening response and the subsequent deformation prediction.

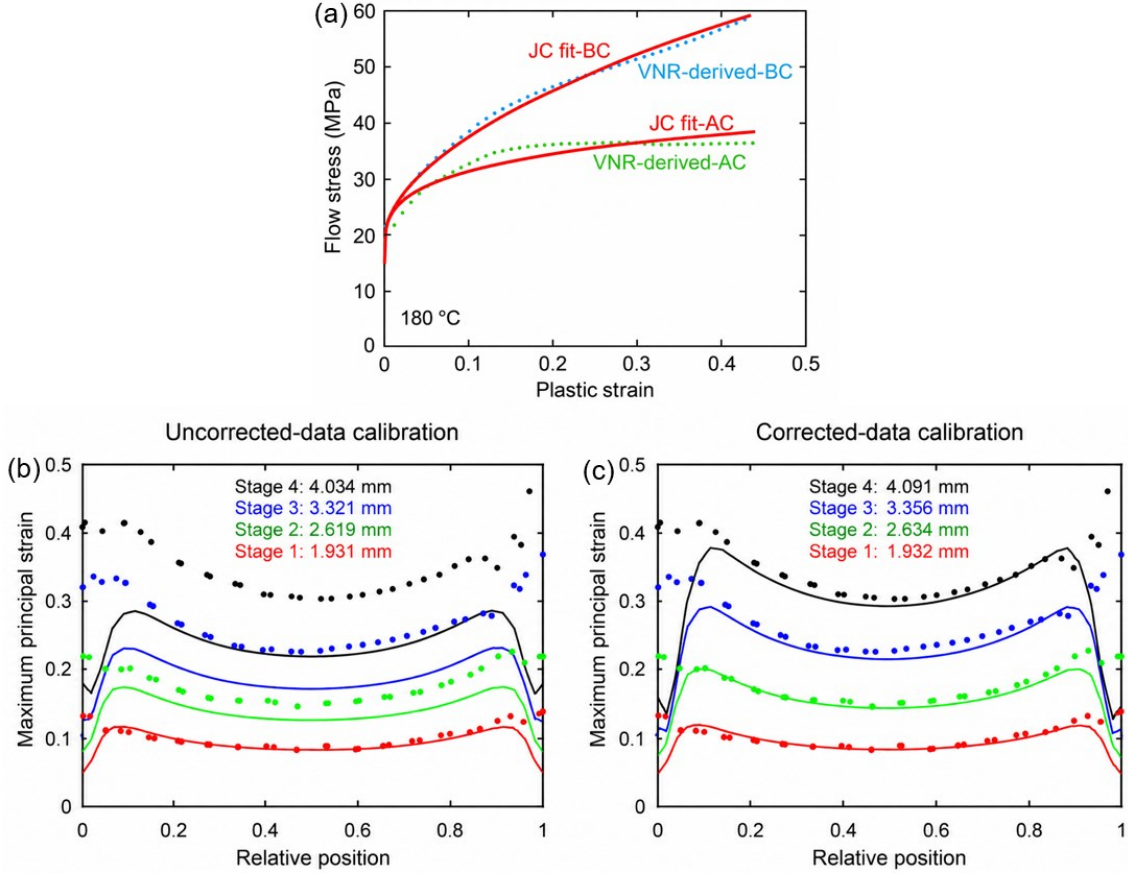


Fig. 13. Effect of stress correction on constitutive identification and predicted ligament strain fields. (a) Johnson–Cook (JC) fits to flow stress–plastic strain curves derived from uncorrected (BC) and corrected (AC) PEEK VNR data at 180 °C; dotted curves denote the VNR-derived responses and solid curves denote the JC fits. (b, c) Maximum-principal-strain profiles predicted using parameters identified from the uncorrected and corrected VNR data, respectively, compared with DIC measurements at four displacement stages. Solid lines denote simulations and dotted symbols denote DIC measurements; the corresponding displacements are given in panel. BC and AC denote before and after correction, respectively.

5.5. Evaluation in additional numerical cases

To assess the correction under additional conditions, two numerical cases were considered: PEEK at 240 °C, which changes the testing temperature while retaining the same material, and PA6 at 160 °C, which additionally changes the material.

The PEEK case uses the temperature-dependent material parameters defined in Table 1. For PA6, the JC parameters fitted to the tensile data reported in [26] are listed in Table 4. In both cases, the same two-step correction equations established in Section 5.3 are applied, without refitting, to the SG-type and ROI-based DIC calculations, and the corrected curves are compared with the prescribed material responses used in the simulations.

Table 4. Material parameters used for the PA6 numerical case at 160 °C, fitted to tensile data from [26].

Properties	Parameters of PA6 at 160 °C
Density (tonne/mm ³)	$\rho = 1.5 \times 10^{-9}$
Modulus (MPa)	$E = 180.13$
Poisson's ratio	$\nu = 0.48$
JC model parameters (A, B, n, m, T_0, T_m)	$A = 11.489,$ $B = 14.531,$ $n = 0.729,$ $m = 1.079,$ $T_0 = 433 \text{ K (160 °C)}$ $T_m = 503 \text{ K (230 °C)}$
Predefined field temperature	433 K (160 °C)

As shown in Fig. 14, the uncorrected SG-type and DIC calculations increasingly exceed the prescribed input responses for both PEEK at 240 °C and PA6 at 160 °C. After correction, both calculated curves are substantially closer to their respective prescribed responses over the analysed deformation range. The same correction equations therefore also reduce the stress error in these additional numerical cases.

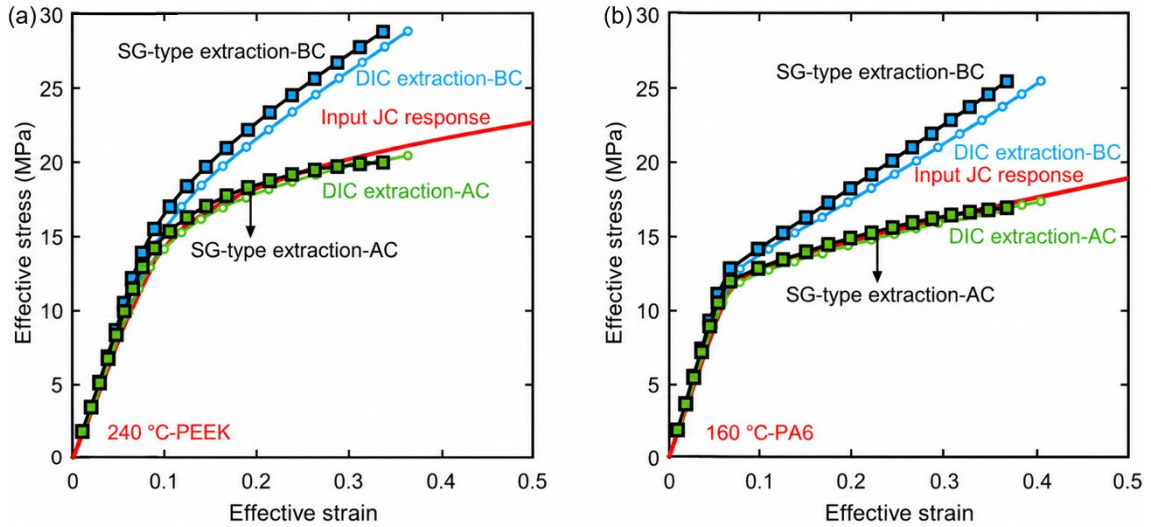


Fig. 14. Additional numerical evaluation of the two-step stress correction. (a) PEEK at 240 °C and (b) PA6 at 160 °C. In each case, the local SG-type and ROI-based DIC responses before (BC) and after (AC) correction are compared with the prescribed JC material response. BC and AC denote before and after correction, respectively.

5.6. Implications for material characterisation and practical application

The present approach extends VNR stress correction from individual properties such as shear modulus or strength [16–18] to the full VNR stress–strain response. This distinction is important because the full response is used directly for constitutive identification. A

deformation-dependent stress error can therefore be embedded in the identified hardening law if it is not corrected before fitting.

For practical application, the two correction relations are established numerically for the VNR configuration using the ROI-averaged response and the ligament-centre response as successive references. Once established, they are applied during post-processing to the stress calculated from the measured load and selected strain measure. The same procedure can be used with either the local SG-type route or the ROI-based DIC route.

The present study assesses the correction experimentally for the modified PEEK VNR test at 180 °C and numerically for PEEK at 240 °C and PA6 at 160 °C. The correction relations depend on the deformation and stress distributions produced by the test configuration. For substantially different specimen geometries, loading conditions or strain-extraction regions, the corresponding finite element model should therefore be used to re-evaluate the correction relations. Further experimental studies across additional materials and testing conditions can extend the assessment of the method.

6. Conclusions

This study investigated deformation-dependent errors in the stress–strain response obtained from large-deformation V-notched rail (VNR) shear tests and developed a two-step correction based on principal-direction change and non-uniform ligament deformation. The analysis showed that the main discrepancy is concentrated in the stress calculation rather than the strain calculation. Both the local SG-type and ROI-based DIC strain measures remain comparatively close to the ligament-centre response, whereas the corresponding load-derived stresses increasingly exceed the centre-point stress as deformation increases, with relative stress errors of about 12% at an effective strain of 0.10.

The proposed correction addresses the two identified sources of stress-calculation error in two successive steps. The first step uses an ROI-averaged numerical reference to address the contribution associated with principal-direction change. The second uses the ligament-centre response to address the remaining effect associated with non-uniform ligament deformation. After correction, the numerical stress–strain curves closely follow the prescribed material response, and the corrected experimental PEEK VNR curve at 180 °C shows substantially improved agreement with the tensile reference. The same correction relations also reduce the stress error in additional numerical cases for PEEK at 240 °C and PA6 at 160 °C.

The results further show that the deformation-dependent stress error can be carried directly into constitutive identification and subsequent finite element prediction. Fitting the uncorrected VNR response produces a stronger apparent hardening response, whereas using the corrected data reduces this artificial hardening and gives ligament strain distributions that agree more closely with the DIC measurements at larger deformation. The main contribution is therefore the correction of the full VNR stress–strain response before constitutive identification, rather than isolated test properties. This reduces the risk of carrying the conversion error into the identified material model and subsequent predictions.

Appendix A. Supporting derivations

To keep the main text compact, this appendix collects the intermediate derivations supporting the baseline VNR conversion, the shear-kinematics and rotation-angle relations, and the $\pm 45^\circ$ strain-gauge transformation illustrated in the main text.

A.1. Detailed baseline VNR conversion

For completeness, Table A1 gives the intermediate principal-value substitutions used to obtain the baseline conversion in Section 2.3. The measured inputs are the vertical load F , initial ligament area A_0 , and ROI-averaged maximum principal engineering strain $\bar{\varepsilon}_{ROI}^{eng}$. The table shows the conversion to logarithmic strain, the assumed principal values, and the resulting equivalent strain and stress.

Table A1. Detailed baseline VNR conversion from measured load and ROI-averaged maximum principal engineering strain.

Variables	Calculation
Measured variables	Vertical Load (F) Initial cross-section area (A_0) Mean value of maximum principal strains (engineering strain) within the ROI (ε_{ROI}^{eng}) captured by DIC software
Logarithmic strain	$\varepsilon^t = \ln(1 + \varepsilon_{ROI}^{eng})$
Principal strains	$\varepsilon_1 = \varepsilon^t$ $\varepsilon_2 = 0$ $\varepsilon_3 = -\varepsilon^t$
Effective strain	$\bar{\varepsilon} = \frac{2}{\sqrt{3}} \varepsilon^t$
Baseline stress	$\sigma^t = \frac{F}{A_0} (1 + \varepsilon_{ROI}^{eng})$
Principal stresses	$\sigma_1 = \sigma^t$ $\sigma_2 = 0$ $\sigma_3 = -\sigma^t$
Effective stress	$\bar{\sigma} = \sqrt{3} \sigma^t$

A.2. Shear kinematics and rotation-angle derivation

Fig. 5 defines the symmetric-shear and simple-shear constructions used in Section 3.1. The intermediate steps are given here so that the main text can retain only the relations needed to follow the stress-correction method.

For the symmetric-shear construction, the in-plane mapping, incompressibility condition and resulting deformation gradient can be written compactly as

$$\begin{aligned}
 X_1^d &= X_1^o + X_2^o \cdot \tan\left(\frac{\gamma_{m,p}}{2}\right); \quad X_2^d = X_1^o \cdot \tan\left(\frac{\gamma_{m,p}}{2}\right) + X_2^o, \\
 X_3^d &= \frac{X_3^o}{1 - \tan^2\left(\frac{\gamma_{m,p}}{2}\right)}, \\
 \mathbf{X}^d &= \begin{bmatrix} 1 & \tan\left(\frac{\gamma_{m,p}}{2}\right) & 0 \\ \tan\left(\frac{\gamma_{m,p}}{2}\right) & 1 & 0 \\ 0 & 0 & \frac{1}{1 - \tan^2\left(\frac{\gamma_{m,p}}{2}\right)} \end{bmatrix} \cdot \mathbf{X}^o, \\
 \mathbf{F}_p &= \frac{\partial \mathbf{X}^d}{\partial \mathbf{X}^o} = \begin{bmatrix} 1 & \tan\left(\frac{\gamma_{m,p}}{2}\right) & 0 \\ \tan\left(\frac{\gamma_{m,p}}{2}\right) & 1 & 0 \\ 0 & 0 & \frac{1}{1 - \tan^2\left(\frac{\gamma_{m,p}}{2}\right)} \end{bmatrix}.
 \end{aligned} \tag{A1}$$

In Eq. (A1), the first two relations define the in-plane mapping, the third enforces incompressibility through the thickness, and the fourth gives the resulting deformation gradient. Because this deformation gradient is symmetric, its in-plane principal directions remain fixed at $\pm 45^\circ$ to the coordinate axes for the positive-shear branch shown in Fig. 5a.

For simple shear, the coordinate mapping is

$$X_1^d = X_1^o; \quad X_2^d = \tan(\gamma_{m,s}) \cdot X_1^o + X_2^o; \quad X_3^d = X_3^o. \tag{A2}$$

Differentiating Eq. (A2) gives the simple-shear deformation gradient reported as Eq. (3) in the main text. The in-plane principal directions of the stretch tensor $\mathbf{U}$ in Eq. (4) follow from its eigenvectors. An unnormalised eigenvector and the associated stretch-related orientation relations are

$$\begin{aligned}
 \hat{\mathbf{v}}_1 &= \begin{bmatrix} \sqrt{1+b^2} + b \\ 1 \\ 0 \end{bmatrix}, \\
 \tan(\theta_s^U) &= \frac{1}{\sqrt{1+b^2} + b} = \sqrt{1+b^2} - b, \\
 \tan(\theta_s^{oU}) &= \tan(\theta_s^U - \theta_s^o) = \frac{\tan(\theta_s^U) - \tan(\theta_s^o)}{1 + \tan(\theta_s^U) \cdot \tan(\theta_s^o)} = \frac{-b}{\sqrt{1+b^2} + 1}.
 \end{aligned} \tag{A3}$$

As the positive shear amount tends to zero, the selected direction approaches 45° , consistent with the reference orientation in Fig. 5. In Eq. (A3), the second and third relations give the orientation after the stretch step and its change relative to the initial 45° direction, respectively.

The subsequent rigid-body rotation and associated half-angle relations are

$$\begin{aligned} \mathbf{R} &= \begin{bmatrix} \cos(\theta_s^{\text{Ud}}) & -\sin(\theta_s^{\text{Ud}}) & 0 \\ \sin(\theta_s^{\text{Ud}}) & \cos(\theta_s^{\text{Ud}}) & 0 \\ 0 & 0 & 1 \end{bmatrix}, \\ \tan(\theta_s^{\text{Ud}}) &= b, \\ \tan\left(\frac{\theta_s^{\text{Ud}}}{2}\right) &= \frac{\sin(\theta_s^{\text{Ud}})}{1+\cos(\theta_s^{\text{Ud}})} = \frac{b}{\sqrt{1+b^2}+1}, \\ \theta_s^{\text{oU}} &= -\frac{1}{2}\theta_s^{\text{Ud}} = -\arctan\left(\frac{b}{\sqrt{1+b^2}+1}\right). \end{aligned} \tag{A4}$$

In Eq. (A4), the first relation gives the rigid-body rotation tensor, while the remaining relations define the corresponding rotation angle, its half-angle form and its relation to the stretch-induced reorientation. The resulting relation shows that the stretch-related orientation change is one half of the rigid-body rotation in the opposite sense.

Combining Eqs. (A3) and (A4) with the rigid-body rotation gives the net principal-direction change reported as Eq. (5) in the main text. These expressions remain well defined at $b = 0$, where the orientation changes vanish.

A.3. Full $\pm 45^\circ$ strain-gauge transformation

For completeness, the full infinitesimal transformation for the $\pm 45^\circ$ gauge arrangement illustrated in Section 3.2 is

$$\begin{aligned} \varepsilon_{+45^\circ} &= \frac{\varepsilon_x + \varepsilon_y}{2} + \frac{\varepsilon_x - \varepsilon_y}{2} \cos(2 \times (+45^\circ)) + \frac{\gamma_{xy}}{2} \sin(2 \times (+45^\circ)) \\ \varepsilon_{-45^\circ} &= \frac{\varepsilon_x + \varepsilon_y}{2} + \frac{\varepsilon_x - \varepsilon_y}{2} \cos(2 \times (-45^\circ)) + \frac{\gamma_{xy}}{2} \sin(2 \times (-45^\circ)). \end{aligned} \tag{A5}$$

Subtracting the two directional strains in Eq. (A5) gives the conventional engineering shear relation for the $\pm 45^\circ$ gauge arrangement. Here, ε_x and ε_y are the normal engineering strains in the x- and y-directions, respectively, and γ_{xy} is the engineering shear strain. All strain measures are dimensionless.

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Data availability

The data supporting this study are available from the corresponding author upon reasonable request.

CRedit authorship contribution statement

Hongyan Wang: Conceptualization; Methodology; Data curation; Formal analysis; Visualization; Writing – original draft. Zerong Ding: Methodology; Formal analysis; Writing – review & editing. Zhutao Shao: Formal analysis; Writing – review & editing. Chunyi Gao: Visualization; Writing – original draft; Writing – review & editing. Haibao Liu: Supervision; Methodology; Formal analysis; Writing – review & editing. Nan Li: Investigation; Supervision; Conceptualization; Methodology; Formal analysis; Writing – review & editing; Project administration; Funding acquisition.

Declaration of generative AI and AI-assisted technologies in the manuscript preparation process

During the preparation of this work, the authors used ChatGPT (OpenAI) to assist with manuscript revision and improve grammar, clarity and readability. After using this tool, the authors reviewed and edited the content as needed and take full responsibility for the content of the publication.

References

- [1] ASTM International. ASTM D7078/D7078M-12: Standard Test Method for Shear Properties of Composite Materials by V-Notched Rail Shear Method. ASTM International, West Conshohocken, PA, 2012. https://doi.org/10.1520/D7078_D7078M-12
- [2] D.O. Adams, J.M. Moriarty, A.M. Gallegos, et al. The V-Notched Rail Shear Test. Journal of Composite Materials 41(3) (2007) 281–297. <https://doi.org/10.1177/0021998306063369>
- [3] ASTM International. ASTM D4255/D4255M-01(2007): Standard Test Method for In-Plane Shear Properties of Polymer Matrix Composite Materials by the Rail Shear Method. ASTM International, West Conshohocken, PA, 2007. https://doi.org/10.1520/D4255_D4255M-01R07
- [4] I. De Baere, W. Van Paepegem, J. Degrieck. Design of a modified three-rail shear test for shear fatigue of composites. Polymer Testing 27(3) (2008) 346–359. <https://doi.org/10.1016/j.polymertesting.2007.12.006>
- [5] ASTM International. ASTM D5379/D5379M-19e1: Standard Test Method for Shear Properties of Composite Materials by the V-Notched Beam Method. ASTM International, West Conshohocken, PA, 2019. https://doi.org/10.1520/D5379_D5379M-19E01
- [6] D.C. Moreira, L.C.S. Nunes. Comparison of simple and pure shear for an incompressible isotropic hyperelastic material under large deformation. Polymer Testing 32(2) (2013) 240–248. <https://doi.org/10.1016/j.polymertesting.2012.11.005>

- [7] C. Thiel, J. Voss, R.J. Martin, et al. Shear, pure and simple. *International Journal of Non-Linear Mechanics* 112 (2019) 57–72. <https://doi.org/10.1016/j.ijnonlinmec.2018.10.002>
- [8] H. Daiyan, E. Andreassen, F. Grytten, et al. Shear testing of polypropylene materials analysed by digital image correlation and numerical simulations. *Experimental Mechanics* 52(9) (2012) 1355–1369. <https://doi.org/10.1007/s11340-012-9591-7>
- [9] L.C.S. Nunes. Mechanical characterization of hyperelastic polydimethylsiloxane by simple shear test. *Materials Science and Engineering: A* 528(3) (2011) 1799–1804. <https://doi.org/10.1016/j.msea.2010.11.025>
- [10] L.C.S. Nunes. Modified slotted shear test for a thin sheet of solid polymer under large deformations. *Polymer Testing* 45 (2015) 20–30. <https://doi.org/10.1016/j.polymertesting.2015.05.003>
- [11] J.C.A.D. Filho, L.C.S. Nunes. Simple shear response of a thin sheet of polytetrafluoroethylene under large deformations. *Polymer Testing* 78 (2019) 105942. <https://doi.org/10.1016/j.polymertesting.2019.105942>
- [12] G. Hedner, R. Seldén, P. Lagercrantz. Shear test for thermoplastic polymers. *Polymer Engineering & Science* 34(6) (1994) 513–518. <https://doi.org/10.1002/pen.760340609>
- [13] C. G'Sell, A. Dahoun. Evolution of microstructure in semi-crystalline polymers under large plastic deformation. *Materials Science and Engineering: A* 175(1–2) (1994) 183–199. [https://doi.org/10.1016/0921-5093\(94\)91058-8](https://doi.org/10.1016/0921-5093(94)91058-8)
- [14] D.F. Jones, L.R.G. Treloar. The properties of rubber in pure homogeneous strain. *Journal of Physics D: Applied Physics* 8(11) (1975) 1285–1304. <https://doi.org/10.1088/0022-3727/8/11/007>
- [15] K.R. Rajagopal, A.S. Wineman. New universal relations for nonlinear isotropic elastic materials. *Journal of Elasticity* 17(1) (1987) 75–83. <https://doi.org/10.1007/BF00042450>
- [16] H. Ho, M.Y. Tsai, J. Morton, et al. Numerical analysis of the Iosipescu specimen for composite materials. *Composites Science and Technology* 46(2) (1993) 115–128. [https://doi.org/10.1016/0266-3538\(93\)90167-F](https://doi.org/10.1016/0266-3538(93)90167-F)
- [17] J.H. Oh, J.K. Kim, D.G. Lee, et al. Interlaminar shear behavior of thick carbon/epoxy composite materials. *Journal of Composite Materials* 33(22) (1999) 2080–2115. <https://doi.org/10.1177/002199839903302202>
- [18] F. Taheri-Behrooz, H.S. Moghaddam. Nonlinear numerical analysis of the V-notched rail shear test specimen. *Polymer Testing* 65 (2018) 44–53. <https://doi.org/10.1016/j.polymertesting.2017.11.008>
- [19] C. Gao, J. Tran, Z. Ding, B.R.K. Blackman, N. Li. Cohesive-friction transition in compressed unidirectional thermoplastic composite interfaces. *Composites Part B: Engineering* 328 (2027) 114187. <https://doi.org/10.1016/j.compositesb.2026.114187>
- [20] E. Totry, J.M. Molina-Aldareguía, C. González, et al. Effect of fiber, matrix and interface properties on the in-plane shear deformation of carbon-fiber reinforced composites. *Composites Science and Technology* 70(6) (2010) 970–980. <https://doi.org/10.1016/j.compscitech.2010.02.014>
- [21] A. Maurel-Pantel, E. Baquet, J. Bikard, et al. A thermo-mechanical large deformation constitutive model for polymers based on material network description: Application to a semi-crystalline polyamide 66. *International Journal of Plasticity* 67 (2015) 102–126. <https://doi.org/10.1016/j.ijplas.2014.10.004>
- [22] P.J. Rae, E.N. Brown, E.B. Orler. The mechanical properties of poly(ether-ether-ketone) (PEEK) with emphasis on the large compressive strain response. *Polymer* 48(2) (2007) 598–615. <https://doi.org/10.1016/j.polymer.2006.11.032>
- [23] B. Zheng, H. Wang, Z. Huang, et al. Experimental investigation and constitutive modeling of the deformation behavior of Poly-Ether-Ether-Ketone at elevated temperatures. *Polymer Testing* 63 (2017) 349–359. <https://doi.org/10.1016/j.polymertesting.2017.08.020>
- [24] H. Wang, Z. Ding, X. Chen, H. Liu, N. Li. Experimental characterisation and constitutive modelling of the intra-ply tensile and shear properties of unidirectional fibre reinforced thermoplastics (UD FRTPs) under solid-state stamp forming conditions. *Composites Part A: Applied Science and Manufacturing* 179 (2024) 108034. <https://doi.org/10.1016/j.compositesa.2024.108034>
- [25] D. Barba, A. Arias, D. Garcia-Gonzalez. Temperature and strain rate dependences on hardening and softening behaviours in semi-crystalline polymers: Application to PEEK. *International Journal of Solids and Structures* 182–183 (2020) 205–217. <https://doi.org/10.1016/j.ijsolstr.2019.08.021>

- [26] H. Wang, H. Liu, Z. Ding, et al. Experimental and constitutive modelling studies of semicrystalline thermoplastics under solid-state stamp forming conditions. *Polymer* 228 (2021) 123939. <https://doi.org/10.1016/j.polymer.2021.123939>
- [27] F. Chen, H. Ou, S. Gatea, H. Long. Hot tensile fracture characteristics and constitutive modelling of polyether-ether-ketone (PEEK). *Polymer Testing* 63 (2017) 168–179. <https://doi.org/10.1016/j.polymertesting.2017.07.032>
- [28] D. Garcia-Gonzalez, A. Rusinek, T. Jankowiak, A. Arias. Mechanical impact behavior of polyether-ether-ketone (PEEK). *Composite Structures* 124 (2015) 88–99. <https://doi.org/10.1016/j.compstruct.2014.12.061>
- [29] A. Abbasnezhad, A. Khavandi, J. Fitoussi, et al. Influence of loading conditions on the overall mechanical behavior of polyether-ether-ketone (PEEK). *International Journal of Fatigue* 109 (2018) 83–92. <https://doi.org/10.1016/j.ijfatigue.2017.12.010>
- [30] M.C. Boyce, D.M. Parks, A.S. Argon. Large inelastic deformation of glassy polymers. Part I: Rate dependent constitutive model. *Mechanics of Materials* 7(1) (1988) 15–33. [https://doi.org/10.1016/0167-6636\(88\)90003-8](https://doi.org/10.1016/0167-6636(88)90003-8)
- [31] E.M. Arruda, M.C. Boyce. A three-dimensional constitutive model for the large stretch behavior of rubber elastic materials. *Journal of the Mechanics and Physics of Solids* 41(2) (1993) 389–412. [https://doi.org/10.1016/0022-5096\(93\)90013-6](https://doi.org/10.1016/0022-5096(93)90013-6)
- [32] A.D. Mulliken, M.C. Boyce. Mechanics of the rate-dependent elastic–plastic deformation of glassy polymers from low to high strain rates. *International Journal of Solids and Structures* 43(5) (2006) 1331–1356. <https://doi.org/10.1016/j.ijsolstr.2005.04.016>
- [33] R.B. Dupaix, M.C. Boyce. Finite strain behavior of poly(ethylene terephthalate) (PET) and poly(ethylene terephthalate)-glycol (PETG). *Polymer* 46(13) (2005) 4827–4838. <https://doi.org/10.1016/j.polymer.2005.03.083>
- [34] J.S. Bergström, M.C. Boyce. Constitutive modeling of the large strain time-dependent behavior of elastomers. *Journal of the Mechanics and Physics of Solids* 46(5) (1998) 931–954. [https://doi.org/10.1016/S0022-5096\(97\)00075-6](https://doi.org/10.1016/S0022-5096(97)00075-6)
- [35] K. Liu, J.-H. Zheng, Z. Shan, et al. Tensile deformation behavior and phenomenological constitutive model of PEEK over a wide temperature range. *Materials & Design* 258 (2025) 114672. <https://doi.org/10.1016/j.matdes.2025.114672>
- [36] B. Ding, C. Ni, Z. Zhao, et al. Constitutive model of PEEK taking into account changes in temperature, strain rate, and damage potential. *Polymer* 345 (2026) 129557. <https://doi.org/10.1016/j.polymer.2026.129557>
- [37] Y. Tian, C. Ni, B. Ding, et al. The constitutive model and damage response of PEEK under high temperature condition. *Engineering Fracture Mechanics* 332 (2026) 111740. <https://doi.org/10.1016/j.engfracmech.2025.111740>
- [38] M.A.N. Dewapriya, J.W. Gillespie Jr., E.J. Pineda, B.A. Bednarczyk. Atomistic modeling of strain rate and temperature dependent tensile response of amorphous PEEK. *Polymer* 356 (2026) 130099. <https://doi.org/10.1016/j.polymer.2026.130099>
- [39] Z. Li, J. Fan, G. Zhang. A new physics-motivated constitutive model of hyperelastic polymer networks. *International Journal of Mechanical Sciences* 314 (2026) 111366. <https://doi.org/10.1016/j.ijmecsci.2026.111366>
- [40] J. Guo, Z. Gao, J. Wang. Thermo-viscoelastic-viscoplastic model for precursor fibers and its application to TCPAs. *International Journal of Mechanical Sciences* 313 (2026) 111316. <https://doi.org/10.1016/j.ijmecsci.2026.111316>
- [41] J. Kim, D.H. Vo, J.-W. Han, et al. Functionality-dependent yield surface asymmetry in crosslinked epoxy polymers. *International Journal of Mechanical Sciences* 313 (2026) 111317. <https://doi.org/10.1016/j.ijmecsci.2026.111317>
- [42] Z. Ding, H.R. Attar, H. Wang, H. Liu, N. Li. Integrating convolutional neural network and constitutive model for rapid prediction of stress-strain curves in fibre reinforced polymers: A generalisable approach. *Materials & Design* 241 (2024) 112849. <https://doi.org/10.1016/j.matdes.2024.112849>

- [43] M.C. Serna Moreno, S. Horta Muñoz, J. García-Delgado. Comparison of the in-plane shear response of a FRP obtained by biaxial tension-compression test and standard methods. *Composites Science and Technology* 276 (2026) 111519. <https://doi.org/10.1016/j.compscitech.2026.111519>
- [44] B. Pan, K. Qian, H. Xie, et al. Two-dimensional digital image correlation for in-plane displacement and strain measurement: A review. *Measurement Science and Technology* 20(6) (2009) 062001. <https://doi.org/10.1088/0957-0233/20/6/062001>
- [45] F. Hild, S. Roux. Digital image correlation: From displacement measurement to identification of elastic properties – a review. *Strain* 42(2) (2006) 69–80. <https://doi.org/10.1111/j.1475-1305.2006.00258.x>
- [46] B. Pan. Digital image correlation for surface deformation measurement: Historical developments, recent advances and future goals. *Measurement Science and Technology* 29(8) (2018) 082001. <https://doi.org/10.1088/1361-6501/aac55b>
- [47] Z. Shao, N. Li, J. Lin, T.A. Dean. Strain measurement and error analysis in thermo-mechanical tensile tests of sheet metals for hot stamping applications. *Proceedings of the Institution of Mechanical Engineers, Part C: Journal of Mechanical Engineering Science* 232(11) (2018) 1994–2008. <https://doi.org/10.1177/0954406217714011>
- [48] M. Ganapathy, N. Li, J. Lin, M. Abspoel, D. Bhattacharjee. A novel grip design for high-accuracy thermo-mechanical tensile testing of boron steel under hot stamping conditions. *Experimental Mechanics* 58 (2018) 243–258. <https://doi.org/10.1007/s11340-017-0333-8>
- [49] L. Yu, B. Pan. Overview of high-temperature deformation measurement using digital image correlation. *Experimental Mechanics* 61(7) (2021) 1121–1142. <https://doi.org/10.1007/s11340-021-00723-8>
- [50] B. Pan, L. Yu, Q. Zhang. Review of single-camera stereo-digital image correlation techniques for full-field 3D shape and deformation measurement. *Science China Technological Sciences* 61 (2018) 2–20. <https://doi.org/10.1007/s11431-017-9090-x>
- [51] X. Shao, Z. Chen, X. Dai, et al. Camera array-based digital image correlation for high-resolution strain measurement. *Review of Scientific Instruments* 89(10) (2018) 105110. <https://doi.org/10.1063/1.5050187>
- [52] M. Rossi, P. Lava, F. Pierron, et al. Effect of DIC spatial resolution, noise and interpolation error on identification results with the virtual fields method. *Strain* 51 (2015) 206–222. <https://doi.org/10.1111/str.12134>
- [53] R. Danda, X. Wu, S. Mao, et al. Phase-augmented digital image correlation for high-accuracy deformation measurement: Theory, validation, and application to constitutive law learning. *Journal of the Mechanics and Physics of Solids* 198 (2025) 106051. <https://doi.org/10.1016/j.jmps.2025.106051>
- [54] B. Li, S. Zhou, Q. Ma, S. Ma. Physics-Informed Neural Network Based Digital Image Correlation Method. *Experimental Mechanics* 65 (2025) 221–240. <https://doi.org/10.1007/s11340-024-01139-w>
- [55] D. Miller, K. Shah, S. Sockalingam, et al. Uncertainty analysis of acceleration and velocity fields measured via DIC. *International Journal of Mechanical Sciences* 302 (2025) 110599. <https://doi.org/10.1016/j.ijmecsci.2025.110599>
- [56] E. Seidi, S.F. Miller, F. Kaviari, et al. Novel method to assess anisotropy in formability using DIC. *International Journal of Mechanical Sciences* 285 (2025) 109782. <https://doi.org/10.1016/j.ijmecsci.2024.109782>
- [57] T. Hou, D. Wang, Y. Wang, K. Zheng, Y. Li. Characterization and modelling of non-uniform hot deformation behavior for laser-welding joints. *International Journal of Mechanical Sciences* 302 (2025) 110571. <https://doi.org/10.1016/j.ijmecsci.2025.110571>
- [58] J.-A. Tröger, C. Steinweller, S. Hartmann. Identification, uncertainty quantification and validation of orthotropic material properties for additively manufactured polymers. *Mechanics of Materials* 197 (2024) 105100. <https://doi.org/10.1016/j.mechmat.2024.105100>
- [59] S.R. Varedi, B. Buffel, F. Desplentere. A Digital twin framework for Visco-Hyperelasticity calibration: Experiment and simulation. *International Journal of Mechanical Sciences* 296 (2025) 110310. <https://doi.org/10.1016/j.ijmecsci.2025.110310>
- [60] G.-W. Kim, H.-J. Rho, Y.-W. Kim, J.-S. Song, W.T. Kim, D.-H. Kim, J.-W. Park, H.-S. Kim. Development of the modified V-notched rail shear test for carbon fiber reinforced composites with multi-scale

- simulation method considering microscopic failure points. *Composites Part A: Applied Science and Manufacturing* 202 (2026) 109535. <https://doi.org/10.1016/j.compositesa.2025.109535>
- [61] S. Avril, M. Bonnet, A.S. Bretelle, et al. Overview of identification methods of mechanical parameters based on full-field measurements. *Experimental Mechanics* 48(4) (2008) 381–402. <https://doi.org/10.1007/s11340-008-9148-y>
- [62] S. Cooreman, D. Lecompte, H. Sol, et al. Elasto-plastic material parameter identification by inverse methods: Calculation of the sensitivity matrix. *International Journal of Solids and Structures* 44(13) (2007) 4329–4341. <https://doi.org/10.1016/j.ijsolstr.2006.11.024>
- [63] J. Réthoré. A fully integrated noise robust strategy for the identification of constitutive laws from digital images. *International Journal for Numerical Methods in Engineering* 84(6) (2010) 631–660. <https://doi.org/10.1002/nme.2908>
- [64] J.M.P. Martins, A. Andrade-Campos, S. Thuillier. Comparison of inverse identification strategies for constitutive mechanical models using full-field measurements. *International Journal of Mechanical Sciences* 145 (2018) 330–345. <https://doi.org/10.1016/j.ijmecsci.2018.07.013>
- [65] F. Pierron, M. Grédiac. Towards Material Testing 2.0. A review of test design for identification of constitutive parameters from full-field measurements. *Strain* 57(1) (2021) e12370. <https://doi.org/10.1111/str.12370>
- [66] F. Pierron. Material Testing 2.0: A brief review. *Strain* 59(3) (2023) e12434. <https://doi.org/10.1111/str.12434>
- [67] A. Marek, F.M. Davis, F. Pierron. Sensitivity-based virtual fields for the non-linear virtual fields method. *Computational Mechanics* 60(3) (2017) 409–431. <https://doi.org/10.1007/s00466-017-1411-6>
- [68] A. Marek, F.M. Davis, J.-H. Kim, et al. Experimental validation of the sensitivity-based virtual fields for identification of anisotropic plasticity models. *Experimental Mechanics* 60 (2020) 639–664. <https://doi.org/10.1007/s11340-019-00575-3>
- [69] M. Rossi, F. Pierron, M. Štamborská. Application of the virtual fields method to large strain anisotropic plasticity. *International Journal of Solids and Structures* 97–98 (2016) 322–335. <https://doi.org/10.1016/j.ijsolstr.2016.07.015>
- [70] A. Lattanzi, F. Barlat, F. Pierron, et al. Inverse identification strategies for the characterization of transformation-based anisotropic plasticity models with the non-linear VFM. *International Journal of Mechanical Sciences* 173 (2020) 105422. <https://doi.org/10.1016/j.ijmecsci.2020.105422>
- [71] A. Schowtjak, R. Schulte, T. Clausmeyer, et al. ADAPT — A Diversely Applicable Parameter Identification Tool: Overview and full-field application examples. *International Journal of Mechanical Sciences* 213 (2022) 106840. <https://doi.org/10.1016/j.ijmecsci.2021.106840>
- [72] M. Gonçalves, M.G. Oliveira, S. Thuillier, et al. Key performance indicators for heterogeneous mechanical tests. *International Journal of Mechanical Sciences* 264 (2024) 108821. <https://doi.org/10.1016/j.ijmecsci.2023.108821>
- [73] D.E. Ricciardi, D.T. Seidl, B.T. Lester, et al. Bayesian optimal experimental design for constitutive model calibration. *International Journal of Mechanical Sciences* 265 (2024) 108881. <https://doi.org/10.1016/j.ijmecsci.2023.108881>
- [74] Y. Zhang, S. Gothivarekar, M. Conde, et al. Enhancing the information-richness of sheet metal specimens for inverse identification of plastic anisotropy through strain fields. *International Journal of Mechanical Sciences* 214 (2022) 106891. <https://doi.org/10.1016/j.ijmecsci.2021.106891>
- [75] M. Ryser, J. Steffen, B. Berisha, M. Bambach. Integrating multiple samples into full-field optimization of yield criteria. *International Journal of Mechanical Sciences* 265 (2024) 108880. <https://doi.org/10.1016/j.ijmecsci.2023.108880>
- [76] M. Conde, S. Coppieters, A. Andrade-Campos. Strategies for automatic constitutive model selection and recommendation. *International Journal of Mechanical Sciences* 264 (2024) 108813. <https://doi.org/10.1016/j.ijmecsci.2023.108813>
- [77] T. Xu, Z. Wang, Y. Hu, et al. A FEM-based direct method for identification of Young’s modulus and boundary conditions in three-dimensional linear elasticity from local observation. *International Journal of Mechanical Sciences* 237 (2023) 107797. <https://doi.org/10.1016/j.ijmecsci.2022.107797>

- [78] T. Xu, Z. Wang, Y. Hu, et al. A multiple-data-based direct method for inverse problem in three-dimensional linear elasticity. *International Journal of Mechanical Sciences* 259 (2023) 108600. <https://doi.org/10.1016/j.ijmecsci.2023.108600>
- [79] L. Rose, A. Menzel. Optimisation based material parameter identification using full field displacement and temperature measurements. *Mechanics of Materials* 145 (2020) 103292. <https://doi.org/10.1016/j.mechmat.2019.103292>
- [80] J. Neggers, F. Mathieu, F. Hild, S. Roux. Simultaneous full-field multi-experiment identification. *Mechanics of Materials* 133 (2019) 71–84. <https://doi.org/10.1016/j.mechmat.2019.03.001>
- [81] S.S. Fayad, E.M.C. Jones, D.T. Seidl, et al. On the importance of direct-levelling for constitutive material model calibration using digital image correlation and finite element model updating. *Experimental Mechanics* 63(3) (2023) 467–484. <https://doi.org/10.1007/s11340-022-00926-7>
- [82] B. Chen, S. Coppieters. Meshfree digital image correlation using element free Galerkin method: Theory, algorithm and validation. *Experimental Mechanics* 63 (2023) 517–528. <https://doi.org/10.1007/s11340-022-00930-x>
- [83] B. Chen, B. Starman, M. Halilović, et al. Finite Element Model Updating for Material Model Calibration: A Review and Guide to Practice. *Archives of Computational Methods in Engineering* 32 (2025) 2035–2112. <https://doi.org/10.1007/s11831-024-10200-9>
- [84] S.S. Fayad, E.M.C. Jones, D.T. Seidl, J. Lambros. Identification uncertainty in inverse material model parameter determination: A sensitivity-based decision process for load path selection. *Strain* 61(3) (2025) e70007. <https://doi.org/10.1111/str.70007>
- [85] D.E. Ricciardi, D.T. Seidl, B.T. Lester, et al. Advancements in constitutive model calibration: Leveraging the power of full-field DIC measurements and in situ load path selection for reliable parameter inference. *Strain* 61(4) (2025) e70008. <https://doi.org/10.1111/str.70008>
- [86] R. Haustrate, F. Hild, A. Fau. Identifying heterogeneous elastic parameters from full-field measurements with reduced bases. *International Journal of Mechanical Sciences* 305 (2025) 110682. <https://doi.org/10.1016/j.ijmecsci.2025.110682>
- [87] S. Hong, H. Kim, M.-G. Lee. Anisotropic plasticity identification: Integrated FE and sensitivity-based virtual fields method. *International Journal of Mechanical Sciences* 306 (2025) 110815. <https://doi.org/10.1016/j.ijmecsci.2025.110815>
- [88] G. Zhu, X. Hu, R. Bao, W.Q. Chen. Continuous high-throughput characterization of mechanical properties via deep learning. *International Journal of Mechanical Sciences* 291–292 (2025) 110137. <https://doi.org/10.1016/j.ijmecsci.2025.110137>
- [89] T. Xu, M. Li, Z. Wang, et al. A direct method to identify Young's moduli and boundary conditions of the heterogeneous material. *International Journal of Mechanical Sciences* 285 (2025) 109777. <https://doi.org/10.1016/j.ijmecsci.2024.109777>
- [90] F. Emami, S.A. Khan, A.J. Gross. A new performance indicator for heterogeneous elastoplastic material test specimens. *International Journal of Mechanical Sciences* 313 (2026) 111271. <https://doi.org/10.1016/j.ijmecsci.2026.111271>
- [91] A. Jafari, K. Vlachas, E. Chatzi, J.F. Unger. A Bayesian framework for constitutive model identification via use of full field measurements, with application to heterogeneous materials. *Computer Methods in Applied Mechanics and Engineering* 433A (2025) 117489. <https://doi.org/10.1016/j.cma.2024.117489>
- [92] S. Meng, A.A.K. Yousefi, S. Avril. Machine-learning-based virtual fields method: Application to anisotropic hyperelasticity. *Computer Methods in Applied Mechanics and Engineering* 434 (2025) 117580. <https://doi.org/10.1016/j.cma.2024.117580>
- [93] S. Ghoul, M. Flaschel, S. Kumar, L. De Lorenzis. A topology optimisation framework to design test specimens for one-shot identification or discovery of material models. *Journal of the Mechanics and Physics of Solids* 203 (2025) 106210. <https://doi.org/10.1016/j.jmps.2025.106210>
- [94] Y. Zhao, H. Li, H. Zhou, H.R. Attar, T. Pfaff, N. Li. A review of graph neural network applications in mechanics-related domains. *Artificial Intelligence Review* 57 (2024) 315. <https://doi.org/10.1007/s10462-024-10931-y>

- [95] J. Vievering, S. Ong, A. Fok, et al. Experimental characterization of spatial randomness of elastic properties of heterogeneous materials. *Experimental Mechanics* 65(8) (2025) 1341–1355. <https://doi.org/10.1007/s11340-025-01218-6>
- [96] H.N. Nguyen, L. Chamoin. Model and mesh selection from a mCRE functional in the context of parameter identification with full-field measurements. *Computational Mechanics* 76(1) (2025) 251–277. <https://doi.org/10.1007/s00466-025-02598-1>
- [97] D. Sun, S. Taguchi, I. Niki, et al. The virtual fields method for identifying viscoelastic properties based on stress-sensitivity virtual fields. *Mechanics of Time-Dependent Materials* 29 (2025) 43. <https://doi.org/10.1007/s11043-025-09781-0>
- [98] Z. Huang, C. Antion, F. Toussaint, et al. Identification for elastoplastic constitutive parameters of 316L stainless steel lattice structures using finite element model updating and integrated digital image correlation. *Mechanics of Materials* 202 (2025) 105232. <https://doi.org/10.1016/j.mechmat.2024.105232>
- [99] Z. Wei, S. Gerke, M. Brünig. Novel uniaxial and biaxial reverse experiments for material parameter identification in an advanced anisotropic cyclic plastic-damage model. *Mechanics of Materials* 205 (2025) 105294. <https://doi.org/10.1016/j.mechmat.2025.105294>
- [100] T.T. Nguyen, E. Gargadennec, Y.-M. Luo, L. Chevalier. Improved inverse identification of anisotropic polymer properties via FEMU and VFM with sensitivity analysis. *Polymer Engineering & Science* 66(2) (2026) 689–702. <https://doi.org/10.1002/pen.70150>
- [101] Y. Liu, M. Chen, Q. Zeng. Physics-informed identification of stress fields and thermo-viscoplastic model parameters for metals from full-field data under impact loading. *Computer Methods in Applied Mechanics and Engineering* 449B (2026) 118568. <https://doi.org/10.1016/j.cma.2025.118568>
- [102] R. Bouclier, R. Bonnet-Eymard, E. Rabineau, J. Réthoré. PINN-Based identification of spatially varying elastic moduli from experimental full-field displacement data. *Computer Methods in Applied Mechanics and Engineering* 455 (2026) 118874. <https://doi.org/10.1016/j.cma.2026.118874>
- [103] E. Sakaridis, C.C. Roth, B. Jordan, D. Mohr. Post-Necking full-field FEMU identification of anisotropic plasticity from flat notched tension experiments. *International Journal of Solids and Structures* 305 (2024) 113076. <https://doi.org/10.1016/j.ijsolstr.2024.113076>