

Tomographic observation and modelling of three-dimensional fibre waviness in prepreg draping

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Abstract

Draping of carbon fibre preregs is widely used to form composite components with complex geometries, but imperfections generated during this process become locked into the cured composite and can strongly affect its structural performance. Here, we use X-ray tomography to investigate the formation of fibre waviness in preregs subjected to controlled bending deformations representative of typical draping operations. The measurements reveal a three-dimensional (3D), higher-order buckling mode that develops perpendicular to the plane of bending and is therefore inaccessible to conventional surface imaging and digital image correlation (DIC). We systematically investigate how the wavelength of this instability depends on consolidation pressure, ply thickness, layup and fibre type. A laminate model is developed to provide a mechanistic description of the instability and the resulting fibre waviness. The model shows excellent quantitative agreement with the tomographic measurements and reveals how the buckling wavelength is governed by the material and geometric properties of the prepreg. Importantly, a comparison between model predictions and experiments enables quantitative inference of mechanical properties of soft, uncured preregs that are otherwise difficult to measure directly. Together, these results provide a mechanistic framework for understanding and predicting the formation of fibre waviness during prepreg draping.

Keywords: *Digital volume correlation, fibre waviness, forming, instability*

1. Introduction

Carbon fibre-reinforced polymer composites are widely used in aerospace structures because of their high specific stiffness and strength. A critical stage in the manufacture of these materials is the forming and draping of uncured prepreg laminates into complex three-dimensional (3D) geometries, which are subsequently cured to form the final structure. Hot Drape Forming (HDF) [1,2] is one example of such a manufacturing process. The quality and structural performance of the final cured composite are strongly dependent on the deformation and loading imposed during the uncured forming stage; even small imperfections generated during forming can become locked into the material upon curing and subsequently degrade its mechanical performance. Fibre waviness and wrinkling are among the most common and critical forming defects and can significantly reduce the stiffness and load-carrying capacity of composite structures [3–5].

Forming of uncured prepregs induces complex deformation modes that typically involve combinations of intra- and inter-ply shear, bending, and compaction or consolidation of the prepreg laminate [6]. The compaction and resin-flow mechanisms that occur during forming depend strongly on prepreg architecture, resin viscosity and tooling geometry [7–9], which in turn influence the initiation and evolution of forming defects [10]. In thick laminates, consolidation-induced reductions in thickness can generate excess ply length which, together with fibre inextensibility and inter-ply constraints, may lead to fibre waviness and wrinkling [11,12]. Wrinkle formation has been associated with a range of instability mechanisms [13–16]. For example, Lin et al. [17] showed that out-of-plane wrinkling during forming of unidirectional prepreg layups can be triggered by a combination of in-plane shear and ply compression.

Considerable effort has been devoted to understanding wrinkle formation during automated composite manufacturing [18,19]. Bakhshi and Hojjati [20] demonstrated that wrinkle growth during automated fibre placement is strongly influenced by the viscoelastic behaviour of uncured prepregs and may continue to evolve with time after deposition. Rajan et al. [21] used stereo digital image correlation (DIC) to quantify wrinkle morphology and showed that wrinkle amplitude and wavelength depend strongly on steering conditions and process parameters. However, such experimental studies rely predominantly on surface observations and therefore cannot directly reveal the internal deformation fields associated with wrinkle initiation and evolution, including complex interactions between neighbouring plies. An understanding of these mechanisms requires volumetric, full-field measurements capable of resolving deformation at the ply scale.

X-ray computed tomography (XCT), when combined with digital volume correlation (DVC) [22–24], provides a powerful means of measuring 3D displacement fields and the corresponding strains within opaque materials. Carbon fibre prepregs are particularly well suited to this approach because their intrinsic microstructural heterogeneity, including X-ray contrast between fibres, resin and inherent defects providing volumetric texture that can be exploited for volume correlation. Previous studies have successfully employed micro-XCT to investigate impregnation dynamics, porosity evolution and void-formation mechanisms in uncured prepregs during processing [25–27]. For example, Mehdikhani et al. [27] used micro-XCT measurements to demonstrate that void evolution in multidirectional carbon fibre/epoxy composites is closely coupled to the local fibre architecture. Such studies illustrate the ability of XCT-based measurements to reveal internal deformation and damage mechanisms that are inaccessible to surface-based techniques.

Here, we use in situ XCT together with DVC to obtain full-field measurements of deformation and strain within uncured carbon fibre prepreg laminates subjected to bending deformations that are representative of forming operations. The volumetric measurements reveal a previously unreported mode of fibre waviness formation in which periodic, short-wavelength undulations develop out of the plane of bending. This deformation mode is inherently difficult to detect using conventional surface-based measurements and may therefore have been overlooked in previous studies. Through a systematic experimental investigation of laminate layup, geometry and processing conditions, combined with numerical calculations, we identify the mechanism responsible for the formation of this waviness and establish the material, geometric and processing parameters that govern its development.

2. Full-field 3D strain measurements under 4-point bending

We investigated the deformation of uncured prepregs under 4-point bending as a relatively simple yet representative analogue of a draping operation. These 4-point bending tests were conducted on beams of length $l = 50$ mm and width w (Fig. 1a) consolidated under a pressure p prior to loading (see Supplementary S1 for details of the manufacture of the beams). In all, we considered 9 series of specimens listed in Table 1. In all cases, the plies comprised the 8552 matrix and we varied the beam width w , consolidation pressure p , ply layup, thickness of the plies and the fibre type. The reference case was an IM7/8552 prepreg beam, consolidated under a pressure $p = 0.02$ MPa, with unidirectional plies of equal thickness $t_{\text{ply}} \sim 140$ μm and arranged in a cross-ply arrangement $[(0/90)_{10}/0]$ so that the beam thickness is $h \sim 3$ mm. The ply layup is given in terms of the orientation θ of the fibres within the plies with respect to the longitudinal axis of the beam. Subsequently, we denote the thickness of a ply within the beam by t_θ where θ is the angle of the fibres with respect to the longitudinal direction of the beam. Thus, the $[(0_2/90_2)_5/0_2]$ beam (series 4 in Table 1) comprises plies of thickness $t_0 = t_{90} \sim 2t_{\text{ply}}$.

Table 1: List of all the beam configurations investigated in this study. We list the layup configuration, consolidation pressure, beam width, and fibre type. All beams were of length $l = 50$ mm with the 8552 matrix. The ply angles are defined with respect to the longitudinal direction of the beam.

Series	Fibre type	Lay-up	Width w (mm)	Consolidation pressure p (MPa)	Main parameter investigated
1	IM7	$[(0/90)_{10}/0]$	4	0.02	Reference
2	IM7	$[(0/90)_{10}/0]$	2, 3, 4, 6, 8	0.02	Beam width
3	IM7	$[(0/90)_{10}/0]$	4	0.02, 0.1, 0.5	Pressure
4	IM7	$[(0_2/90_2)_5/0_2]$	4	0.02	Ply thickness
5	IM7	$[(0_2/90)_{6}/0_2]$	4	0.02	Ply thickness
6	IM7	$[(0/90_2)_7/0]$	4	0.02	Ply thickness
7	IM7	$[(90/+45/0/-45)_5/90]$	4	0.02	Fibre orientation
8	IM7	$[(+45/-45)_{10}/+45]$	4	0.02	Fibre orientation
9	AS4	$[(0/90)_{10}/0]$	4	0.02	Fibre type

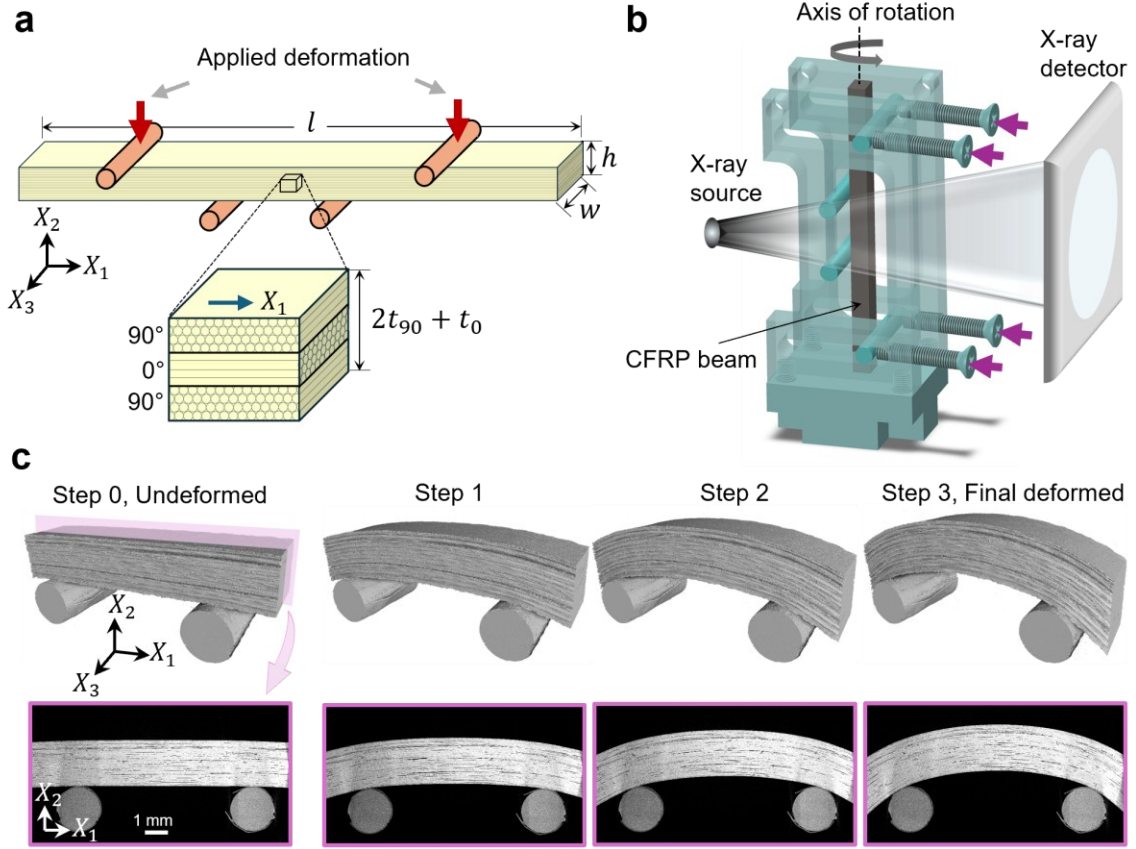


Figure 1: (a) Geometry of the prepreg beam used in this study. The global co-ordinate system (X_1, X_2, X_3) is also indicated. (b) Sketch of the in situ XCT test setup for 4-point bending, and (c) XCT reconstructions of the undeformed and deformed reference $[(0/90)_{10}/0]$ beam at 3 stages of deformation. Images of a central $X_1 - X_2$ plane (indicated in step 0) are also included for all stages of the deformation.

The in situ XCT 4-point bending setup made from Perspex (Fig. 1b) was specifically designed to fit within the XCT setup close to the X-ray source, thereby ensuring maximum geometric magnification and minimal attenuation due to the loading setup. Moreover, following [28], the setup was designed such that the longitudinal beam axis was vertical. This ensured a near axisymmetric geometry about the vertical rotation axis, which significantly enhanced the quality of the tomographic reconstructions. The setup comprised two pairs of cylindrical rollers of diameter $D = 3$ mm with the inner and outer pairs spaced 12 mm and 37 mm apart, respectively. Loading was imposed by manually incrementally adjusting the four screws that displaced the outer roller pair (marked in Fig. 1b) in 3 steps of 3 mm each to achieve a final roller displacement of 9 mm. Full tomographic scans were performed at each of the 3 loading steps as well as the initial undeformed state at a resolution of $9.5 \mu\text{m}$. This resolution is sufficient to observe individual plies and perform strain measurements at ply-level (see Supplementary Section S1 for further experimental details). The full 3D displacement field, with respect to the undeformed state, at each of these loading steps was inferred from the tomographs via DVC using the *Elastix* software [24,29,30]. A multistage DVC approach was employed to enable correlation over the large deformations. The hyperparameters of the DVC method were identified using a synthetic displacement field approach, and the fidelity of the DVC inference (Supplementary S2) was confirmed through DIC measurements (Supplementary S1 and S2) of the surface displacements. These displacements were then used to evaluate the Green-Lagrange strain $e_{ij} \equiv 0.5(F_{ki}F_{kj} - \delta_{ij})$ within the volume of the beams, where F_{ij} is the deformation gradient and δ_{ij} the Kronecker delta.

2.1 Observations of the deformation mode of the reference $[(0/90)_{10}/0]$ cross-ply layup

We first present a detailed set of results for the reference cross-ply laminate of width $w = 4$ mm comprising 21 sets of $\theta = 0^\circ$ and 90° plies consolidated under a small pressure $p = 0.02$ MPa (i.e. a $[(0/90)_{10}/0]$ layup and labelled series 1 in Table 1). Corresponding tomographic reconstructions of the deformation of the cross-ply beam over the 3 deformation steps, and in the undeformed state, are included in Fig. 1c along with cross-sectional views of the marked central plane. This volume data is used in the DVC analysis that we now proceed to discuss. Throughout, we use the global co-ordinate system (X_1, X_2, X_3) indicated in Fig. 1a where X_1 is along the longitudinal direction of the undeformed beam and X_2 aligned with the beam thickness h .

Distributions of selected strain components e_{ij} at the final deformed state (on the mid $X_1 - X_2$ plane) are included in Fig. 2a, with the distributions shown on the undeformed configuration. First consider the strain component e_{11} that in theory should have shown the bending strains corresponding to compression in the bottom half and tension in the top half. Instead, this distribution shows rather small strain values fluctuating about zero throughout the beam. This is because, while longitudinal straining corresponding to bending does indeed occur, the high stiffness of the 0° plies in the X_1 -direction implies that the longitudinal strains are small and below the resolution of the DVC. We shall subsequently argue that the observed patchy strain distributions are associated with the growth and collapse of voids within the uncured prepregs. A similar strain pattern is also observed for e_{33} and to some extent e_{22} ; distributions of e_{22} show large strains only near the edges of the imaged beam around the central loading rollers. The effect of the rollers is clearer when we consider the distributions of the shear strain e_{12} . The beam is divided into two halves with large (and approximately spatially uniform) positive and negative shear strains. This suggests that much of the deformation is due to shear rather than bending. We rationalise that by noting that friction between the rollers and the beam implies that even in this 4-point bending setup, pure bending is not imposed on these beams under the large deformation considered here. Rather, a combination of bending and shear stresses are imposed with the low shear stiffness and strength of the uncured cross-ply resulting in large shear deformations. The spatial heterogeneity of the uncured plies also implies that the combination of the bending and shearing deformations induces voids within the beam to open and close. These local deformations are evident in the distributions of (e_{11}, e_{22}, e_{33}) . The empty (white) regions visible in the strain maps in Fig. 2a correspond to voided regions of the beam, where no material strain is evaluated by DVC. To better quantify the opening and closing of voids, we also include in Fig. 2a distributions of the volumetric strain $e_{\text{vol}} \equiv \det(F_{ij}) - 1$. Volumetric strains of the order of $\pm 10\%$ are observed to fluctuate throughout the beams which given the incompressibility of the matrix, correspond to opening and closing of voids.

Finally, consider the distributions of the shear strain e_{23} . These are out-of-plane shear strains that cannot be inferred from surface 3D DIC measurements. Again, strains varying between $\pm 10\%$ are observed, but now a periodic pattern is evident in both the X_1 and X_2 -directions. More importantly, the amplitude and wavelengths of the strain fluctuations are nearly constant, suggesting that the periodic variation in e_{23} is a signature of a previously unreported instability that is triggered by the bending of uncured cross-ply laminates. Visual observations suggest that the wavelengths of the strain fluctuations are $\lambda_1 \sim 2$ mm and $\lambda_2 \sim 1$ mm in the X_1 and X_2 -directions, respectively; we shall quantify these wavelengths more systematically in Section 2.3.

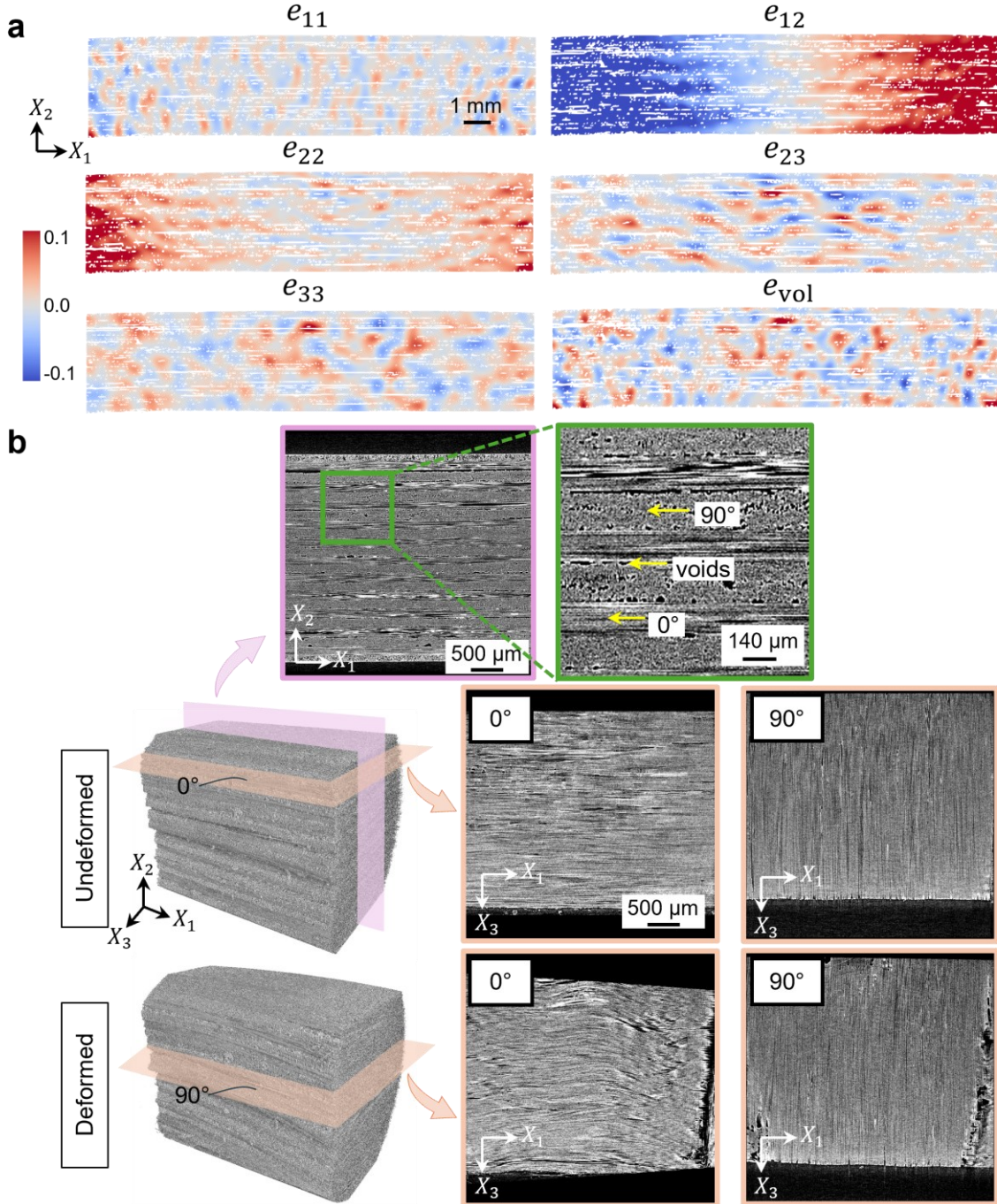


Figure 2: (a) Measurements of the distributions of selected components of the Green-Lagrange strain in the reference $[(0/90)_{10}/0]$ beam over a section between the two central rollers. The strains are shown on the undeformed beam on a central $X_1 - X_2$ plane after the beam had been deformed to its final deformed state with a 9 mm roller displacement. (b) High-resolution XCT reconstructions to show the higher order fibre waviness in the 0° plies corresponding to periodic variation of e_{23} seen in (a). No such waviness is observed in the 90° plies.

To clarify the origins of this periodic shear strain pattern, high-resolution XCT reconstructions were performed on the undeformed and final deformed state (Fig. 2b). The tomographic scans were captured at a resolution of $1.95 \mu\text{m}$ to spatially resolve features smaller than the fibre diameter (the IM7 fibre diameter is $\sim 5 \mu\text{m}$); see Supplementary S1. The central $X_1 - X_2$ plane of the undeformed beam in Fig. 2b clearly resolves the 0° and 90° plies and also the voided regions between and within the plies. The undeformed configuration comprises relatively

straight fibres in both 0° and 90° plies. The observations in Fig. 2b show a clear development of waviness in the X_3 –direction in the 0° plies while the fibres in the 90° plies remain straight and seemingly unaffected in the tomographic images of the deformed state. More importantly, the wavelength of the directly observed 0° fibre waviness (Fig. 2b) matches the periodic $\lambda_1 \sim 2$ mm of the alternating strain field e_{23} in Fig. 2a. This strongly suggests that the periodic e_{23} pattern in Fig. 2a is associated with the formation of waviness in the 0° plies. We emphasise that unlike classical laminate buckling/wrinkling [12] which is a laminate level phenomenon with wavelengths scaling with the structural scale of the laminate, this is a higher-order instability with a short wavelength. Equally, the observed periodic waviness that spans the length of the beam is also unlike a microbuckling instability that results in a highly localised formation of fibre waviness [31,32]. Additional 3-point bending measurements (Supplementary S3) confirmed that the instability that manifests itself with a periodic strain field e_{23} is not restricted to just 4-point bending deformations.

2.2 *The mechanism of fibre waviness formation in the $[(0/90)_{10}/0]$ layup*

Given that this fibre waviness mode is unlike any reported in previous studies, we proceed to investigate it more thoroughly. The strain fields e_{23} on the orthogonal $X_1 - X_2$ and $X_2 - X_3$ planes at the final deformed state are included in Fig. 3a on the undeformed configuration. Zoom-ins where we have marked the approximate locations of the 0° and 90° plies from the deformed tomographic images are also included to help probe the deformation mechanism. Two key observations suggest the mechanism of the formation of fibre waviness in the 0° plies. These observations are: (i) while fibre waviness was only observed in the 0° plies (Fig. 2b), the observations in Fig. 3a suggest that the amplitude of e_{23} is the highest in the 90° plies, and (ii) wavelength $\lambda_2 \sim 4t_0$ with peak e_{23} amplitudes occurring within the 90° plies.

Using these two observations, we propose a mechanism of fibre waviness formation sketched in Fig. 3b. The key recognition is that formation of waviness in the 0° plies is accompanied by extensive shear deformation in the 90° plies. This deformation in the 90° plies is not immediately visible in the tomographs in Fig. 2b as the shear strains e_{23} are parallel to the fibres in the 90° plies and hence do not induce fibre deformation/waviness in those plies. The proposed mechanism is summarised as follows. The imposed deformation induces compressive stresses, triggering an instability involving generation of waviness in the 0° plies. However, given the wavelength $\lambda_2 \sim 4t_0$ with the peak values of e_{23} occurring in the 90° plies (Fig. 3a), we argue that a change in sign of e_{23} occurs in the 0° plies as sketched in Fig. 3b. Our observations in Fig. 3a support this view which is also further confirmed via the detailed finite element (FE) calculations presented in Section 4. A corollary to the change in sign of e_{23} in the 0° plies is that the curvature $\kappa_{23} \sim \partial u_3^2 / \partial X_2^2$ of consecutive 0° plies are out of phase with one another as indicated in Fig. 3b. Since κ_{23} parameterises the phase of the fibre waviness, this sign change predicts a corresponding shift in the waviness phase between consecutive 0° plies. High-resolution XCT scans showing sections through consecutive deformed 0° plies (Fig. 3c) confirm this phase change, in agreement with the mechanism sketched in Fig. 3b.

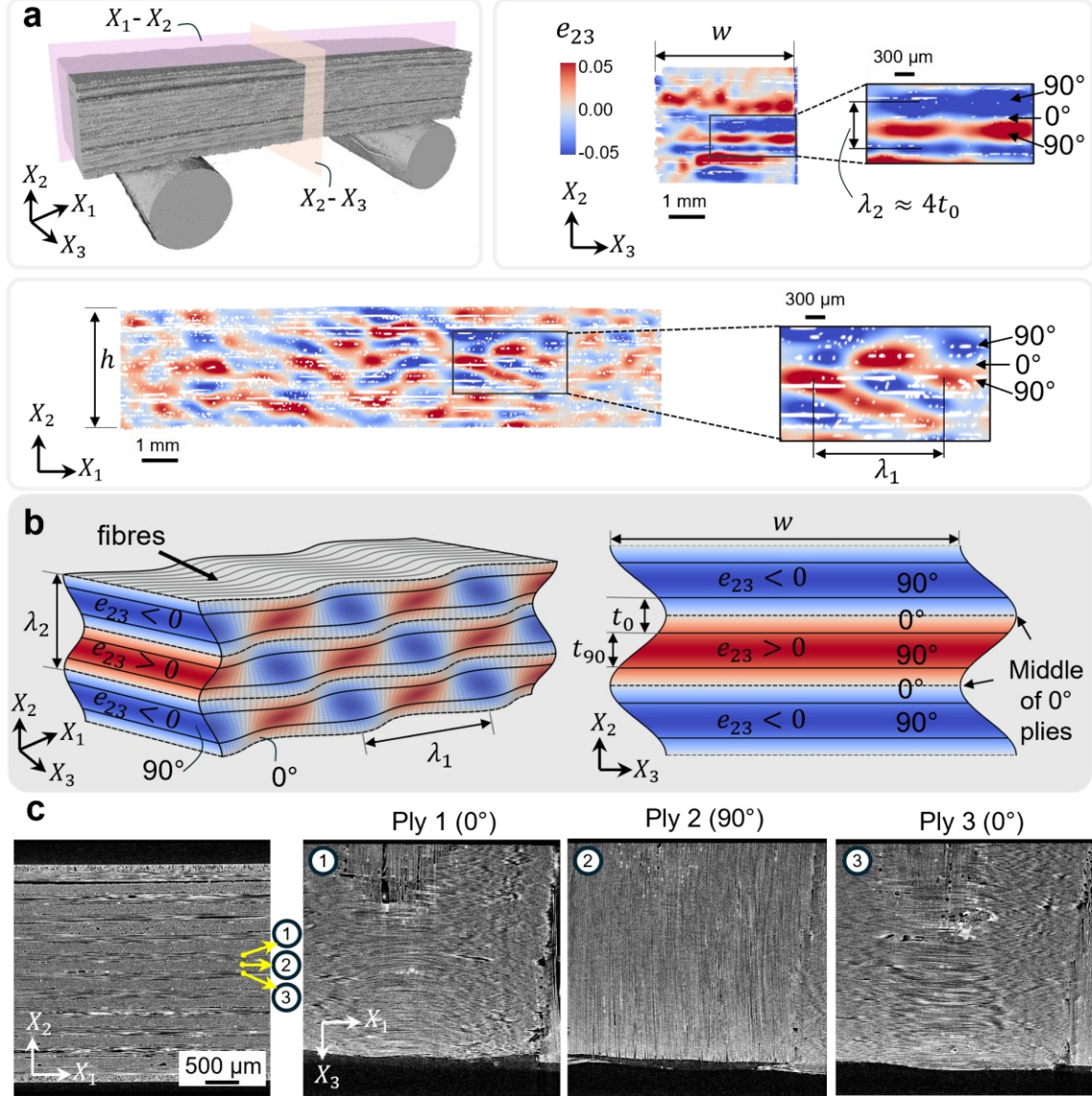


Figure 3: (a) Measurements of the distributions of the strain e_{23} on the indicated orthogonal $X_1 - X_2$ and $X_2 - X_3$ planes at the final deformed state of the reference $[(0/90)_{10}/0]$ beam. The observations are shown on the undeformed configuration and zoom-ins included to indicate the relevant periodic wavelengths. (b) A sketch of the deformation mechanism inferred from the observations of the strain fields. (c) High-resolution XCT reconstructions showing the change in phase of the fibre waviness between consecutive plies, as proposed in (b).

2.3 Quantification of the fibre waviness

The DVC data (Fig. 2a) allows us to measure the wavelengths of the periodic patterns in e_{23} . This information will subsequently permit us to develop an understanding of the dependence of this instability on the processing and topology of the laminate beam. To quantify the wavelengths, we extracted the e_{23} distributions on $X_1 - X_2$ planes of the beam in its final deformed state and plotted these in the undeformed configuration of the beam. These planes were separated by $30 \mu\text{m}$ in the X_3 -direction so that more than 100 planes with distributions of e_{23} were employed in the analysis. Each plane was discretised into a regular grid comprising $(M - 1 \times N - 1)$ rectangles of size $\Delta x \times \Delta y = 9 \mu\text{m} \times 2 \mu\text{m}$ in the X_1 and X_2 -directions, respectively. The measured strains on each of the planes were then interpolated on nodes of this grid and we denote those values as $\varepsilon_{23}(x_m, y_n)$ where (x_m, y_n) are the X_1 and X_2 coordinates of the locations of the grid points labelled $(m = 0, \dots, M - 1, n = 0, \dots, N - 1)$. The

interpolation also fills the gaps associated with voided regions using neighbouring strain values and produces a continuous strain field for subsequent analysis. A comparison between the distributions of e_{23} and the interpolated strains ε_{23} on a selected plane is included in Fig. 4a. For each plane we then performed a discrete Fourier transform (DFT) given by

$$\mathcal{F}(k_1, k_2) = \sum_{m=0}^{M-1} \sum_{n=0}^{N-1} \varepsilon_{23}(x_m, y_n) \exp \left[2\pi i \left(\frac{k_1 m}{M} + \frac{k_2 n}{N} \right) \right], \quad (2.1)$$

and the evaluated the associated power spectrum

$$\mathcal{P}(k_1, k_2) = \frac{1}{(MN)^2} |\mathcal{F}(k_1, k_2)|^2. \quad (2.2)$$

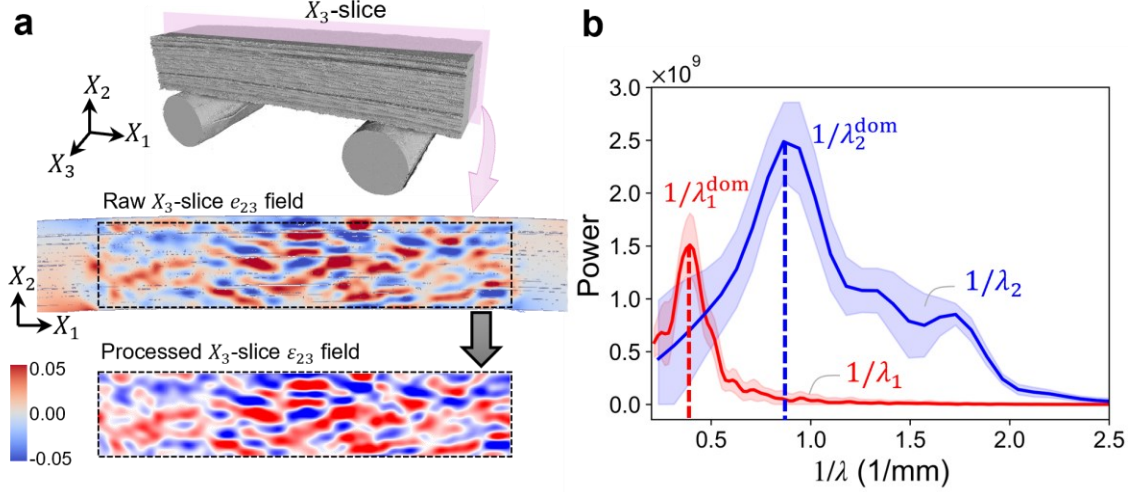


Figure 4: (a) Comparison between the original DVC inferred strains e_{23} and the distribution ε_{23} after projecting e_{23} on a regular rectangular mesh $\Delta x \times \Delta y = 9 \mu\text{m} \times 2 \mu\text{m}$. The data here is shown for a central $X_1 - X_2$ plane of the reference $[(0/90)_{10}/0]$ beam deformed to its final state. This analysis was performed on ~ 100 planes over the width of the beam. (b) The corresponding power spectra $\mathcal{P}_1$ and $\mathcal{P}_2$ for the distribution of the wavelengths λ_1 and λ_2 , respectively extracted by a Fourier analysis of the strains ε_{23} . The shaded band shows the standard deviation of the spectra across the ~ 100 planes and the spectrum for the single plane shown in (a) is also included as a solid line. The modes of the spectrum for this plane are also marked.

The power spectrum can be used to estimate the dominant wavelengths in the X_1 and X_2 -directions as follows. Consider the projections

$$\mathcal{P}_1(k_1) = \sum_{n=0}^{N-1} \mathcal{P}(k_1, k_2), \quad (2.3)$$

and

$$\mathcal{P}_2(k_2) = \sum_{m=0}^{M-1} \mathcal{P}(k_1, k_2). \quad (2.4)$$

These projected spectra for the plane shown in Fig. 4a are included in Fig. 4b as solid lines, where we have converted the frequencies (k_1, k_2) to wavelengths via

$$\lambda_1 \equiv \frac{M\Delta x}{k_1}, \text{ and } \lambda_2 \equiv \frac{N\Delta y}{k_2}. \quad (2.5)$$

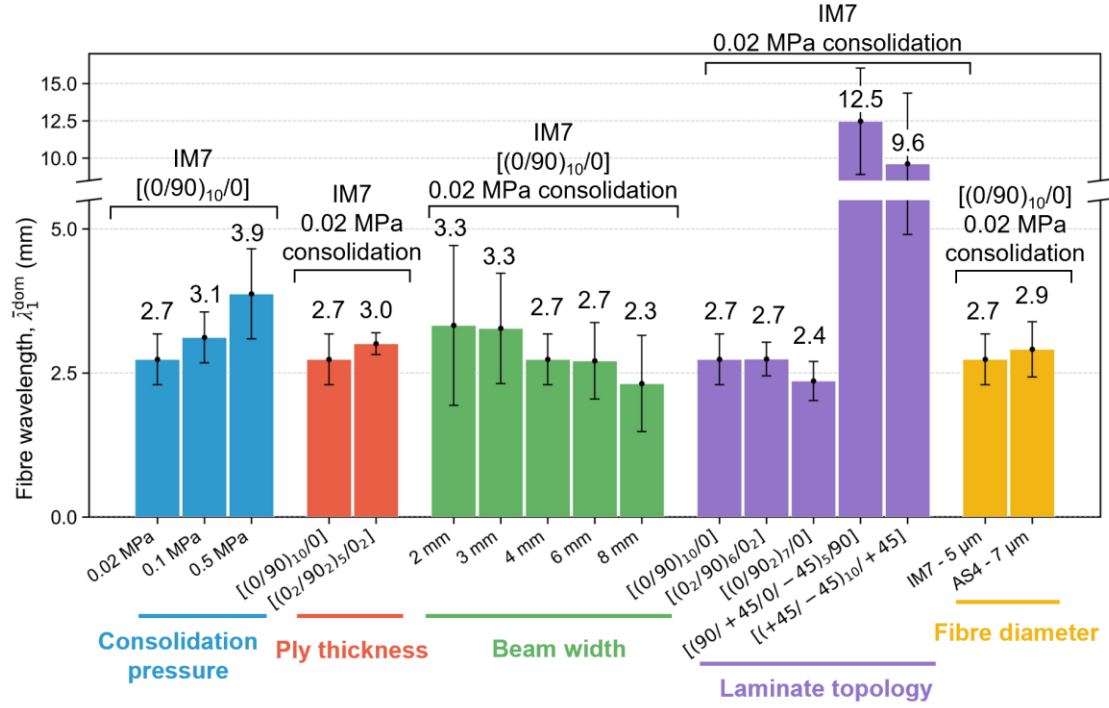


Figure 5: Bar chart summarising the measured mean dominant wavelengths $\bar{\lambda}_1^{\text{dom}}$ over all the beam configurations listed in Table 1. The error bars indicate the standard deviation of λ_1^{dom} over the ~ 100 planes over which the Fourier analysis to extract the wavelengths was conducted. The measurements are divided into 5 groups: effect of (i) consolidation pressure; (ii) ply thickness; (iii) beam width; (iv) ply layup/topology and (v) fibre diameter. Unless otherwise specified, beams have the reference width $w = 4$ mm, consolidated with a pressure $p = 0.02$ MPa and comprise plies with the IM7 fibre.

The shaded band in Fig. 4b indicates the standard deviation of the spectra across the ~ 100 planes over the width of the beam. For each of these planes, we identify the peak of the projected spectra $\mathcal{P}_1$ and $\mathcal{P}_2$ and denote them as λ_1^{dom} and λ_2^{dom} to indicate the dominant wavelengths in the X_1 and X_2 -directions, respectively. For the $[(0/90)_{10}/0]$ reference cross-ply layup (series 1 in Table 1), the mean of the dominant wavelengths across all the imaged planes are $\bar{\lambda}_1^{\text{dom}} \approx 2.7$ mm and $\bar{\lambda}_2^{\text{dom}} \approx 720$ μm . The latter is much smaller and approximately equal to $4t_0$ as discussed above. Thus, while the wavelength in the X_2 -direction is clearly connected to the ply thickness (via the mechanism in Fig. 3b), the factors governing the wavelength in the X_1 -direction remain unclear.

3. Sensitivity of processing, layup and material on development of fibre waviness

To establish the factors that govern the wavelength $\bar{\lambda}_1^{\text{dom}}$, we examine the effect of processing conditions and ply layup. Specifically, we repeated the measurements detailed in Section 2 for the reference $[(0/90)_{10}/0]$ cross-ply system (series 1 in Table 1) for the remaining 8 cases listed in Table 1. A summary of the extracted $\bar{\lambda}_1^{\text{dom}}$ is presented in Fig. 5 (error bars in Fig. 5 correspond to the standard deviation of λ_1^{dom} across all the imaged planes) and divided into 5 groups; viz., effect of (i) consolidation pressure; (ii) ply thickness; (iii) beam width; (iv) ply layup/topology and (v) fibre diameter. In all cases, the matrix was 8552 and the laminates were manufactured from unidirectional plies. Ply thickness was changed (series 4-6 in Table 1) by double stacking plies (i.e. the double thickness plies were of thickness $2t_{\text{ply}} \sim 280$ μm , where $t_{\text{ply}} \sim 140$ μm is the thickness of the prepreg) and fibre diameter controlled by using the 7 μm AS4 fibre rather than the 5 μm IM7 fibre. We emphasize that series 1 in Table 1 will serve as

the reference case, viz. the $[(0/90)_{10}/0]$ cross-ply layup of width $w = 4$ mm comprising IM7 fibres with plies of thickness ~ 140 μm and consolidated with $p = 0.02$ MPa.

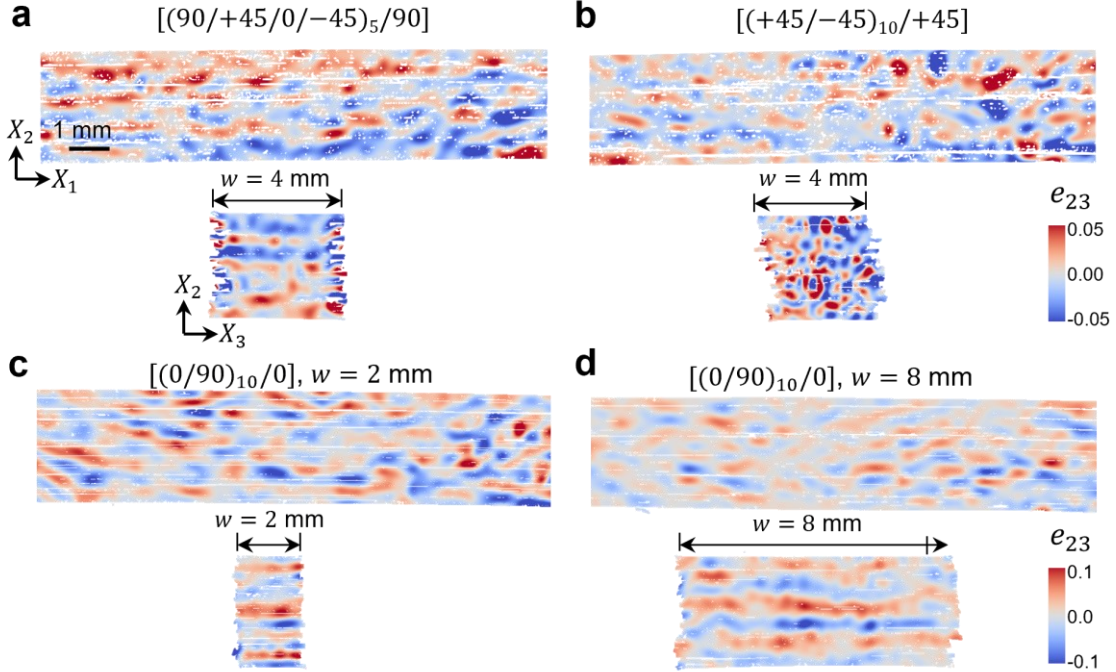


Figure 6: Measurements of the distributions of the strain field e_{23} on central orthogonal $X_1 - X_2$ and $X_2 - X_3$ planes at the final deformed state of the (a) $[(90/+45/0/-45)_5/90]$ and (b) $[(+45/-45)_{10}/+45]$ layups as well as the $[(0/90)_{10}/0]$ beams of width (c) $w = 2$ mm and (d) $w = 8$ mm all consolidated with $p = 0.02$ MPa and made from plies with the IM7 fibre. The observations are shown on the undeformed configuration.

The main message from the summary in Fig. 5 is that changing the layup to include $\pm 45^\circ$ plies essentially eliminates the short wavelength modes in the X_1 -direction. More subtle effects are seen in some of the other cases, with a statistically significant increase in $\bar{\lambda}_1^{\text{dom}}$ observed with increasing consolidation pressure p and decreasing beam width w . We focus on discussing these cases with dataset of the e_{23} distributions for all series in Table 1 completed by including Supplementary Figs. S1 and S2. First consider the $[(90/+45/0/-45)_5/90]$ and $[(+45/-45)_{10}/+45]$ layups. The distributions of e_{23} on a central $X_1 - X_2$ plane and $X_2 - X_3$ are included in Figs. 6a and 6b, respectively. Consistent with the large wavelengths $\bar{\lambda}_1^{\text{dom}}$ indicated in Fig. 5, we see no clear periodic patterns unlike those observed for the reference $[(0/90)_{10}/0]$ case (Fig. 3a). Next for the effect of beam width, we consider the $[(0/90)_{10}/0]$ layup consisting of plies of different widths (series 2 in Table 1). Distributions of e_{23} for beams of width $w = 2$ mm and 8 mm are included in Figs. 6c and 6d, respectively. Now a clear periodic pattern in e_{23} is observed. However, while the quantitative extraction of the wavelengths indicates that $\bar{\lambda}_1^{\text{dom}}$ decreases by ~ 1 mm from $w = 2$ mm to the beams of width $w = 8$ mm (Fig. 5), this dependence on w is not immediately apparent by visual inspection of the images in Figs. 6c and 6d. The reason for this is that the e_{23} patterns have a distribution of wavelengths (Fig. 4b) that require a detailed quantitative analysis to deconvolute. Nevertheless, the dependence of $\bar{\lambda}_1^{\text{dom}}$ on the beam width gives a strong indication that while $\bar{\lambda}_2^{\text{dom}}$ scales with ply thickness as discussed in Section 2, $\bar{\lambda}_1^{\text{dom}}$ is strongly influenced by the beam width. In fact, in all the cross-ply cases, the wavelength $\bar{\lambda}_2^{\text{dom}} \sim 2(t_{90} + t_0)$ and thus the deformation mode

resulting in fibre waviness in the 0° plies is as sketched in Fig. 3b with the ply thickness setting $\bar{\lambda}_2^{\text{dom}}$.

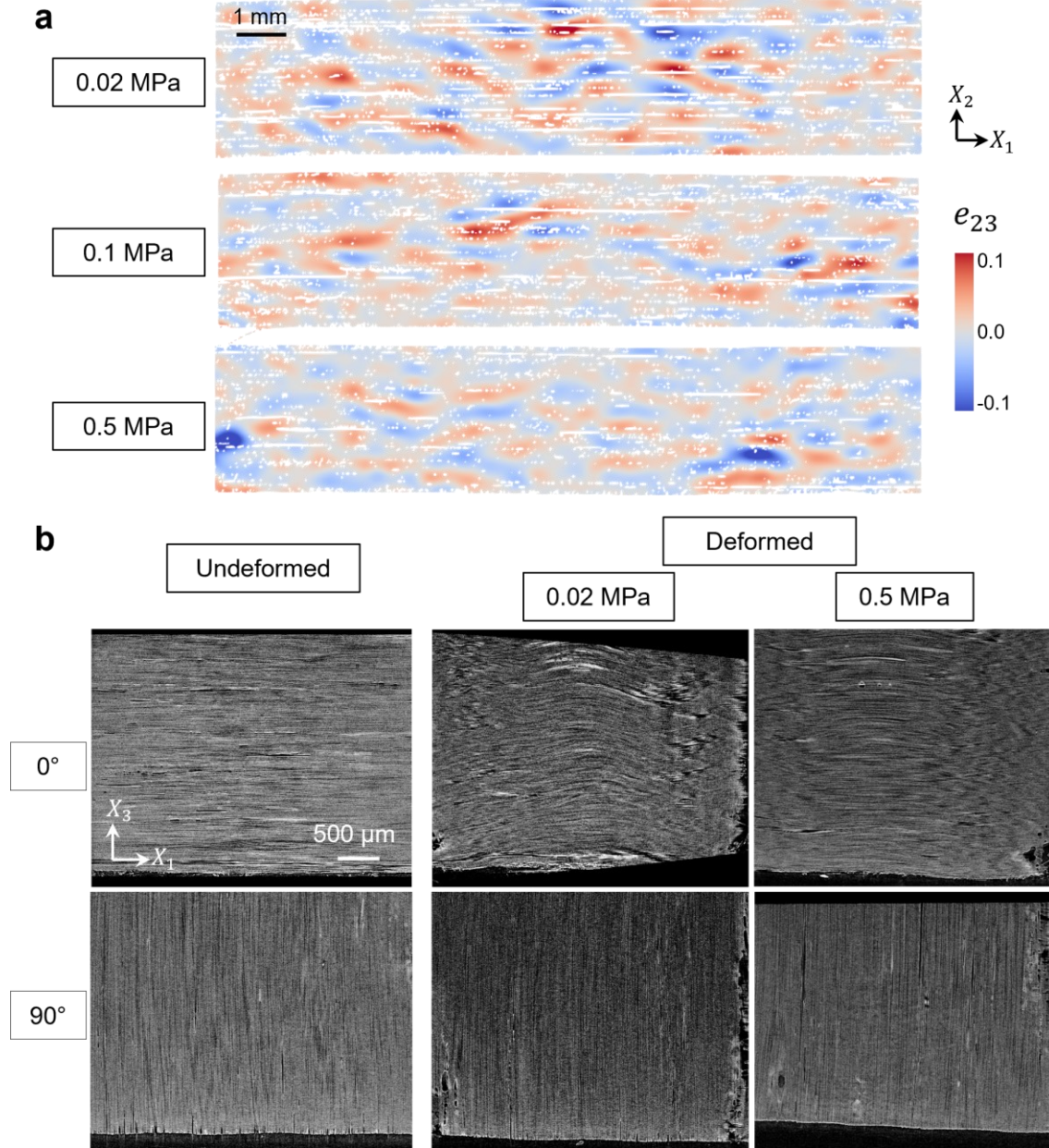


Figure 7: (a) Measurements of the distributions of the strain field e_{23} on a central $X_1 - X_2$ plane at the final deformed state of the reference $[(0/90)_{10}/0]$ beam of width $w = 4$ mm consolidated with three pressures p . The strain distributions are plotted on the undeformed beam configuration. (b) Corresponding high-resolution XCT observations of the 0° and 90° plies in the undeformed state and deformed to the final state. The deformed configuration is shown for beams consolidated with $p = 0.02$ MPa and 0.5 MPa.

Similar observations also emerge from the investigation of the effect of consolidation pressure p on the distributions of e_{23} (Fig. 7a). The quantitative extractions in Fig. 5 indicate that $\bar{\lambda}_1^{\text{dom}}$ increases with increasing p although again this is not immediately apparent in the images. However, what is clear is that the amplitude of e_{23} decreases with increasing p , suggesting that there will also be a decrease in the amplitude of the fibre waviness in the 0° plies. To confirm this, we conducted high-resolution XCT observations (Fig. 7b). At low consolidation pressure,

the 0° plies exhibit pronounced fibre undulations, but the amplitude of the waviness decreases with increasing p .

Finally consider the effect of fibre diameter. The IM7/8552 and AS4/8552 prepregs have the same 8552 matrix and identical fibre volume fractions, with the main differences being the fibre diameter and fibre stiffness, with IM7 being slightly stiffer than AS4. Increasing the fibre diameter from $\sim 5 \mu\text{m}$ for IM7 to $\sim 7 \mu\text{m}$ for AS4 has small influence on $\bar{\lambda}_1^{\text{dom}}$ (Fig. 5). This weak dependence on the fibre-scale properties further confirms that the observed instability is a ply-level buckling mode rather than fibre-level microbuckling. Nevertheless, the parameters that govern $\bar{\lambda}_1^{\text{dom}}$ seem more complex with beam width and consolidation pressure playing a role.

4. Elucidation of the mechanism of the formation of fibre waviness

The observations over a wide range of beam topologies and processing conditions have indicated that in all cross-ply arrangements, fibre waviness develops as manifested in the periodic distribution of e_{23} . The wavelength $\bar{\lambda}_1^{\text{dom}}$ depends on consolidation pressure, ply width and ply thickness. To interpret these dependencies, we develop a simplified finite element (FE) laminate model that contains the essential ingredients of the problem, viz. the axial compressive loading and the ply layup.

4.1 Outline of the model setup for cross-ply laminates comprising plies of equal thickness

Recall that waviness only develops in the 0° plies in the cross-ply arrangements. We thus begin by focusing on the cross-ply laminates with plies of equal thickness. The effect of plies of different thicknesses is discussed in Section 4.5.

To model the laminate, we consider a representative 3-ply arrangement as shown in Fig. 8a where the 0° ply of thickness t_0 is sandwiched between two $\theta = 90^\circ$ also of thickness $t_{90} = t_0$. This minimal setup models the interactions of the 0° ply with the adjacent 90° plies with sufficient accuracy. Further details of the model and material property descriptions are given in Supplementary S4 and here we summarise the key points. In the model, plies are connected to each other by linear elastic cohesive layers with a shear stiffness K_s while the normal displacements of the plies are essentially tied to each other to prevent their separation. This is achieved by setting the normal cohesive stiffness $K_n \gg K_s$. This assumption is consistent with the fact that normal delamination was not observed in the experiments. All the plies have identical properties and are modelled as homogeneous orthotropic linear elastic media and the length of this 3-ply arrangement is set to $L = 50 \text{ mm}$; scoping calculations confirmed that increasing the value of L has a negligible influence on the results presented here. Longitudinal compressive loading was imposed as illustrated in Fig. 8a where all degrees of freedom of the beam at one end were fixed and a displacement $u_1 = U$ imposed at the other end with the remaining degree of freedom constrained, i.e. $u_2 = u_3 = 0$. In addition, overall, the 3-ply arrangement was constrained not to undergo overall straining in the X_2 –direction by setting $u_2 = 0$ on the top and bottom surfaces of the 3-ply arrangement but allowing free movement of these surfaces in the X_1 –direction to allow the compressive deformation to be imposed. (Moreover, consistent with the fact that the beam does not undergo shear deformation the X_3 –direction, we constrained the displacement on the top and bottom surfaces to be equal in the X_3 –direction.) This boundary value problem was analysed in the implicit version of the commercial FE package ABAQUS with the plies modelled using 8-noded elements (C3D8R in the ABAQUS notation) of size $t_0/4$.

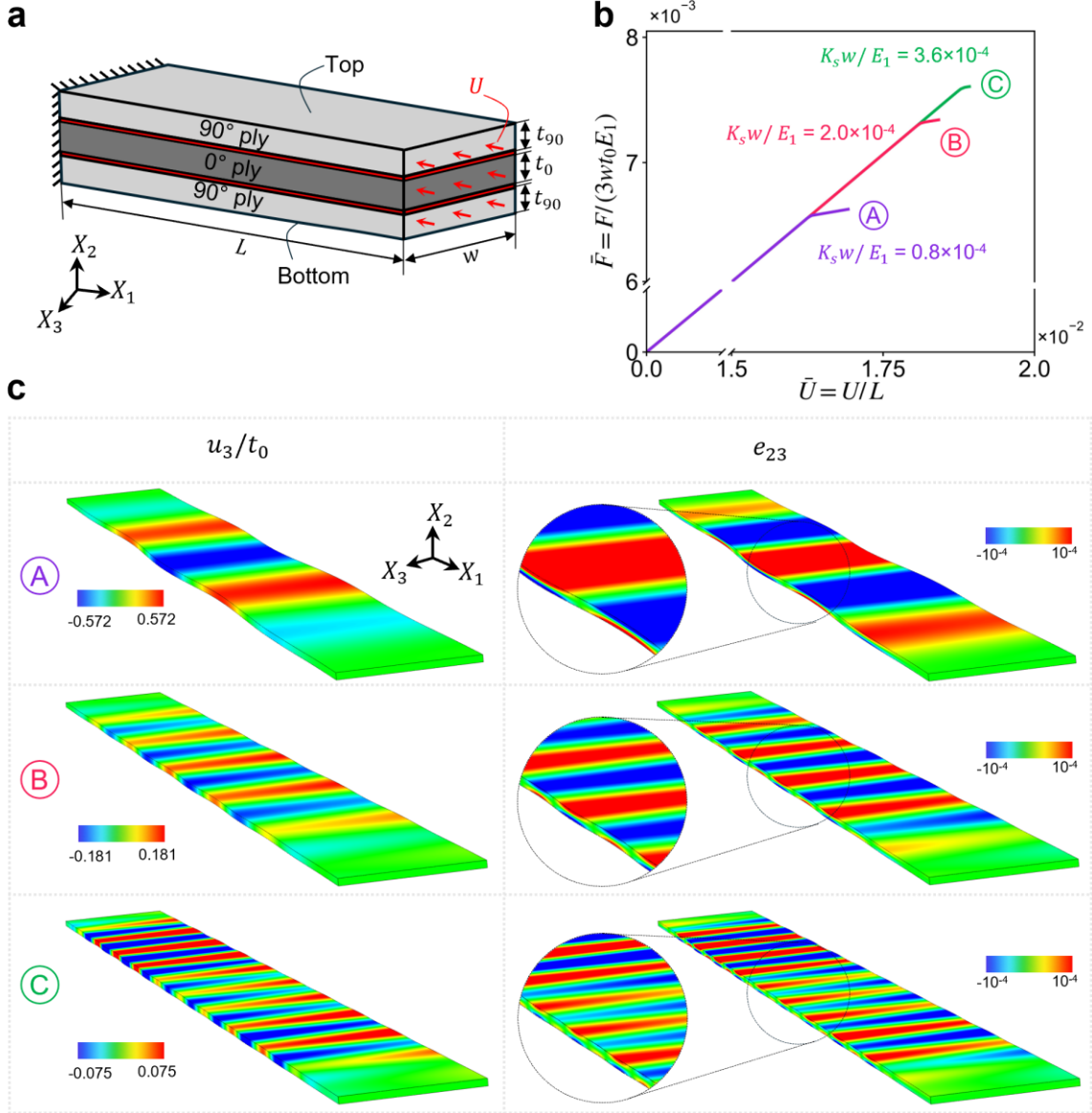


Figure 8: (a) Sketch of the 3-ply arrangement used to analyse the cross-ply laminates with plies of equal thickness $t_0 = t_{90}$. The sketch indicates the cohesive interfaces (red) used to couple the plies and the imposed longitudinal compression. (b) Predictions of the normalised force $\bar{F}$ versus displacement $\bar{U}$ curves for the cross-ply layup for three selected values of $K_s w/E_1$ with $E_1/E_m = 10$, $E_1/G_m = 100$, $w/t_0 = 20$ and $\nu_m = 0.3$. (c) The corresponding distributions of the normalised displacements u_3/t_0 and the strain e_{23} in the 0° ply at the end of the plotted $\bar{F} - \bar{U}$ for three selected values of $K_s w/E_1$. The insets show zoom-ins of the 0° ply to show the change in sign in e_{23} through the thickness of the 0° ply.

Consider a 0° ply modelled as an orthotropic elastic material with 9 independent elastic constants denoted by the Young's modulus (E_1, E_2, E_3), shear moduli (G_{23}, G_{31}, G_{12}) and Poisson's ratios ($\nu_{23}, \nu_{31}, \nu_{12}$) where the subscripts denote the directions of the 0° ply in terms of the global co-ordinate system (Fig. 1a). For uncured plies with a very soft matrix, the stiffness E_1 is governed by the volume fraction of the fibres and their stiffness and therefore we set to $E_1 = 200$ GPa in all cases. Moreover, the stiffnesses (E_2, E_3) and (G_{23}, G_{31}, G_{12}) are set by the matrix and hence it is reasonable to assume that $E_2 = E_3 = E_m$ and $G_{23} = G_{31} = G_{12} = G_m$ and equally $\nu_{23} = \nu_{31} = \nu_{12} = \nu_m$, where the subscript "m" denotes the matrix although we recognise that these stiffnesses are not solely set by the matrix. In any case, the

values (E_m, G_m, ν_m) are uncertain as these low values for an uncured prepreg are difficult to directly measure. In fact, we shall establish and subsequently validate these values by comparing our predictions with observations. The properties of the 90° plies are identical to the 0° plies and obtained by suitable rotation of the co-ordinate system. Similarly, the shear stiffness K_s of the cohesive layers coupling the plies is also largely governed by the uncured matrix and will be estimated via the calculations.

The FE calculations were conducted in two steps. In step 1 an eigenvalue extraction was conducted and approximately 35 independent eigenmodes were extracted. These eigenmodes, equally weighted were then used to perturb the geometry of the 3-ply arrangement in the second step such that the maximum amplitude of the waviness imposed on the beam was $\sim 10^{-4}t_0$. In step 2, a finite strain FE calculation was conducted where an incremental loading $\dot{U}$ was imposed and the work-conjugate force F monitored. The deviation of the load versus displacement curve $F - U$ from its initial linear response provides an indication of the onset of the instability as we shall now proceed to discuss.

4.2 Dimensional analysis and mechanism of deformation

Our primary aim is to determine the wavelength $\bar{\lambda}_1^{\text{dom}}$. Given the geometry of the 3-ply arrangement and the associated material properties, dimensional analysis dictates that the functional form of this wavelength is given by

$$\frac{\bar{\lambda}_1^{\text{dom}}}{t_0} = f\left(\frac{E_1}{G_m}, \frac{K_s w}{E_1}, \frac{w}{t_0}, \frac{E_1}{E_m}, \nu_m\right). \quad (4.1)$$

We shall show in Section 4.3 that over a realistic range of values, $\bar{\lambda}_1^{\text{dom}}/t_0$ is independent of w/t_0 , E_1/E_m and ν_m . Thus, unless otherwise specified, in the subsequent discussion these values are set at reference values of $w/t_0 = 20$, $E_1/E_m = 10$ and $\nu_m = 0.3$.

First consider a cross-ply laminate with $E_1/G_m = 100$ and $K_s w/E_1 = 3.6 \times 10^{-4}$ (case C in Fig. 8b). Prediction of the normalised compressive force $\bar{F} \equiv F/(3wt_0E_1)$ versus $\bar{U} \equiv U/L$ is included in Fig. 8b. At $\bar{F} \approx 7.6 \times 10^{-3}$, the force versus displacement curve deviates sharply from the initial linear response, indicating the onset of the instability. The distribution of the normalised displacements u_3/t_0 in the 0° ply at the end of the plotted $\bar{F} - \bar{U}$ response shown in Fig. 8b is included in Fig. 8c. In addition, corresponding distributions of the strain e_{23} in the 0° ply are also presented in Fig. 8c. Consistent with the observations (Fig. 2a), a periodic distribution of e_{23} is predicted, although the strain values are smaller as the calculation was terminated at the onset of the instability¹. In addition to the periodic pattern of e_{23} in the X_1 –direction, the calculation predicts a change in sign in e_{23} through the thickness of the 0° ply; see the inset in Fig. 8c.

Next consider one of the two additional cases included in Fig. 8b with lower values of $K_s w/E_1 = 0.8 \times 10^{-4}$ (case A) and 2.0×10^{-4} (case B). The normalised load at which the instability sets in increases from A to B to C (i.e. with increasing $K_s w/E_1$) and concurrently the wavelength of the periodic pattern of e_{23} in the X_1 –direction decreases as seen in the u_3/t_0 and e_{23} distributions in Fig. 8c. The predicted decrease in this wavelength is qualitatively consistent with the observation that $\bar{\lambda}_1^{\text{dom}}$ decreases with increasing beam width w (Fig. 5) which in the simulations corresponds to increasing $K_s w/E_1$.

¹ Recall that the calculation assumes linear elastic behaviour of the plies and thus is accurate only to the onset of the instability with the large strains that develop with increasing deformation expected to induce plasticity within the plies.

4.3 Influence of material properties

Our primary aim is to use the simulations to understand the dependence of $\bar{\lambda}_1^{\text{dom}}/t_0$ as expressed in (4.1). With this in mind, for each set of the independent non-dimensional groups in (4.1) we performed a series of calculations by loading the 3-ply arrangement until the onset of the non-linearity of the $\bar{F} - \bar{U}$ curve. The resulting spatial distribution of e_{23} within the 0° ply was then analysed using the same Fourier transform procedure outlined for the measurements in Section 2.3, and the predicted $\bar{\lambda}_1^{\text{dom}}$ extracted. We now report the key outcomes of these simulations. Recognising that the formation of the waviness in the 0° ply is strongly dependent on the interaction of the 0° ply with the adjacent 90° plies, we present the variation of $\bar{\lambda}_1^{\text{dom}}/t_0$ as a function of the non-dimensional group that parametrises the shear coupling between the plies, viz. $K_s w/E_1$.

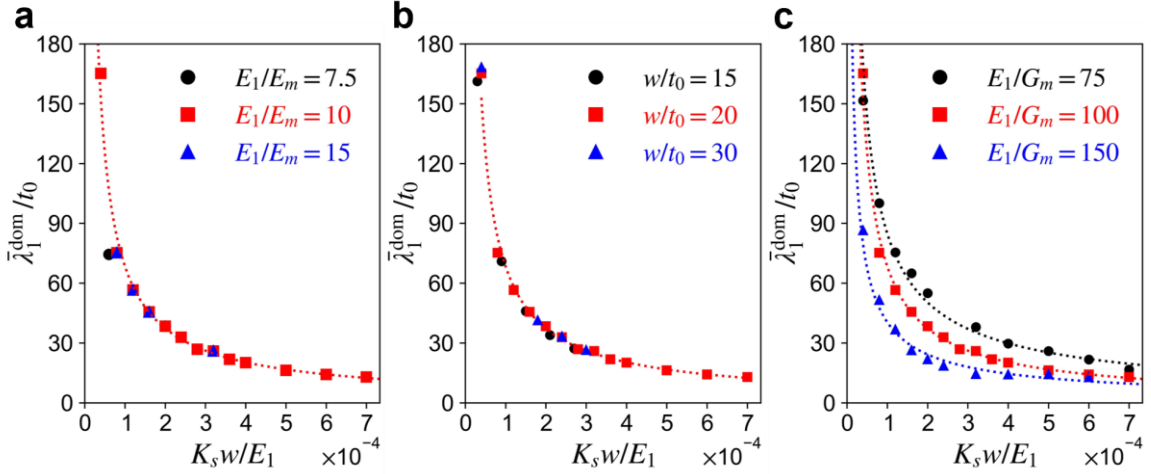


Figure 9: Illustration of the sensitivity of the predictions of $\bar{\lambda}_1^{\text{dom}}/t_0$ to material and geometric parameters for the cross-ply layup with plies of equal thickness and $\nu_m = 0.3$. Predictions of $\bar{\lambda}_1^{\text{dom}}/t_0$ as a function of $K_s w/E_1$ for selected values of (a) E_1/E_m , (b) w/t_0 and (c) E_1/G_m . Unless otherwise specified w/t_0 , E_1/E_m and E_1/G_m were held fixed at their reference values of 20, 10 and 100, respectively.

With w/t_0 and ν_m fixed at their reference values and $E_1/G_m = 95$ we vary E_1/E_m over the range $7.5 \leq E_1/E_m \leq 15$. Predictions of $\bar{\lambda}_1^{\text{dom}}/t_0$ as a function of $K_s w/E_1$ are included in Fig. 9a for three selected values of E_1/E_m in this range and clearly illustrate that the dependence of $\bar{\lambda}_1^{\text{dom}}/t_0$ on E_1/E_m is negligible. This is unsurprising as direct straining in the X_2 and X_3 –directions is small and the formation of fibre waviness in the 0° ply is primarily governed by shearing on the matrix. Next consider the effect of w/t_0 with other values fixed at their reference values. Over the range $15 \leq w/t_0 \leq 30$ we observe that $\bar{\lambda}_1^{\text{dom}}/t_0$ has negligible dependence on w/t_0 (Fig. 9b). This is more surprising since $\bar{\lambda}_1^{\text{dom}}/t_0$ depends so strongly on the beam width w as parameterised by $K_s w/E_1$. We argue that the weak dependence on w/t_0 is because the mode of waviness formation is primarily linked to the change in sign of e_{23} through the ply thickness (Fig. 3b) and thus not strongly dependent on the ratio w/t_0 . For the sake of brevity, we do not show the dependence of $\bar{\lambda}_1^{\text{dom}}/t_0$ on ν_m but it suffices to say that the dependence of $\bar{\lambda}_1^{\text{dom}}/t_0$ on ν_m is negligible.

Finally, we consider the effect of E_1/G_m keeping all other parameters at their reference values. Predictions of the dependence of $\bar{\lambda}_1^{\text{dom}}/t_0$ on $K_s w/E_1$ for three selected values of E_1/G_m are included in Fig. 9c. Given that the mode of fibre waviness involves shearing of the 0° ply, it is

unsurprising that $\bar{\lambda}_1^{\text{dom}}/t_0$ strongly depends on E_1/G_m with the non-dimensional wavelength $\bar{\lambda}_1^{\text{dom}}/t_0$ increasing with decreasing E_1/G_m for a fixed $K_s w/E_1$. Increasing the shear modulus G_m inhibits the short wavelength modes due to greater resistance to the shear deformation and hence $\bar{\lambda}_1^{\text{dom}}/t_0$ increases with decreasing E_1/G_m .

Our discussion here is focussed on $\bar{\lambda}_1^{\text{dom}}/t_0$. Corresponding to the wavelength of the instability there exists a critical force $F = F_c$ at which this instability initiated. The dependencies of the normalised critical force $\bar{F}_c \equiv F_c/(3wt_0E_1)$ corresponding to the material and geometric parameters in Fig. 9 are discussed in Supplementary S4; also see Fig. S3.

4.4 Comparison with measurements for cross-ply laminates with plies of equal thickness

The numerical results in the preceding section have established that (4.1) is well-approximated by

$$\frac{\bar{\lambda}_1^{\text{dom}}}{t_0} \sim g\left(\frac{E_1}{G_m}, \frac{K_s w}{E_1}\right). \quad (4.2)$$

This now allows us to compare predictions with observations and importantly use these comparisons to extract the stiffnesses G_m and K_s – material parameters that are difficult to directly measure in uncured prepreps. In this section we restrict the discussion to cross-ply beams with plies of equal thickness, i.e. $t_0 = t_{90}$.

First consider the cross-ply beam $[(0/90)_{10}/0]$ manufactured with a consolidation pressure $p = 0.02$ MPa and widths in the range $2 \text{ mm} \leq w \leq 8 \text{ mm}$ (series 2 of specimens in Table 1). All these beams should have the same material properties, viz. E_1/G_m and K_s/E_1 . We thus performed a series of calculations to determine $\bar{\lambda}_1^{\text{dom}}/t_0$ as a function of $K_s w/E_1$ for a range of values of E_1/G_m and used the five measured values of $\bar{\lambda}_1^{\text{dom}}/t_0$ to obtain a best fit to the data. Comparison between measurements (with the error bars indicating the standard deviations in Fig. 5) and predictions are included in Fig. 10a for $E_1/G_m = 95$ and $K_s/E_1 = 0.125 \text{ m}^{-1}$. The excellent overall agreement allows us to infer G_m and K_s with the knowledge that the modulus in the fibre direction is known to a good level of accuracy to be $E_1 = 200$ GPa. Setting $E_1 = 200$ GPa implies that the shear modulus of the plies is $G_m = 2.11$ GPa and $K_s = 25$ MPa mm^{-1} .

Next consider the effect of consolidation pressure and ply thickness. The cohesive coupling between plies, characterised here by K_s and the shear stiffness G_m , could both change with consolidation pressure and ply thickness as these are effective properties of the laminate system rather than directly related to just the matrix. Thus, we again performed a series of calculations to infer the change in G_m and K_s with consolidation pressure and ply thickness. Intriguingly, this study revealed that K_s/E_1 is only weakly affected by changes in the ply thickness and consolidation pressure but these changes have a large influence on E_1/G_m . Focusing on the cross-ply beams of width $w = 4$ mm, we then fixed K_s at the value calibrated in Fig. 10a, i.e. $K_s/E_1 = 0.125 \text{ m}^{-1}$ such that $K_s w/E_1 = 5 \times 10^{-4}$ and varied E_1/G_m . This prediction is included as the line in Fig. 10b. First consider the comparison with the measured values of $\bar{\lambda}_1^{\text{dom}}/t_0$ for the $[(0/90)_{10}/0]$ beams consolidated with pressures in the range $0.02 \text{ MPa} \leq p \leq 0.5 \text{ MPa}$.

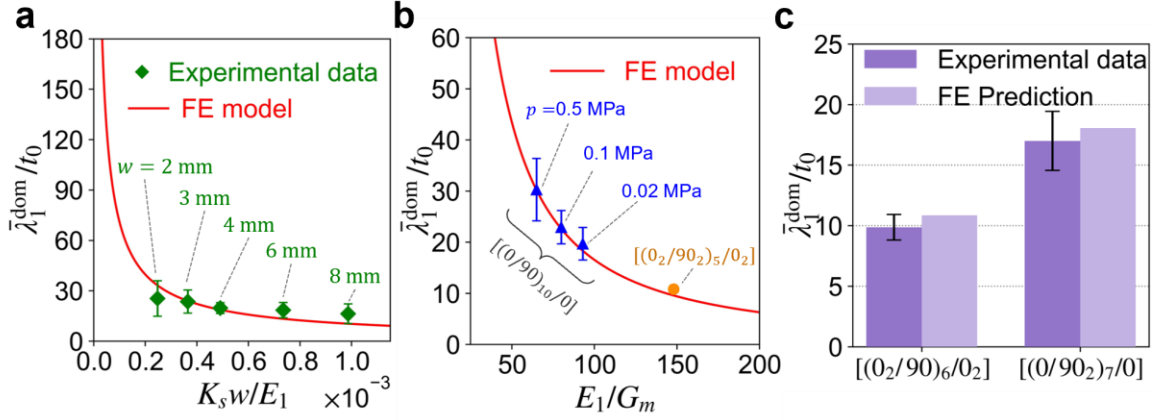


Figure 10: Comparison between the measurements and predictions of $\bar{\lambda}_1^{\text{dom}}/t_0$. Effect of (a) beam width w and (b) consolidation pressure for the $[(0/90)_{10}/0]$ layup. Predictions are shown in (a) as a function of $K_s w/E_1$ with $E_1/G_m = 95$ while the measurements for the $[(0/90)_{10}/0]$ layup consolidated at $p = 0.02$ MPa are plotted using $K_s/E_1 = 0.125 \text{ m}^{-1}$. On the other hand, in (b) the predictions use $K_s w/E_1 = 0.5 \times 10^{-4}$ corresponding to $K_s/E_1 = 0.125 \text{ m}^{-1}$ for the $w = 4$ mm beams and $\bar{\lambda}_1^{\text{dom}}/t_0$ is shown as a function of E_1/G_m . Measurements of $\bar{\lambda}_1^{\text{dom}}/t_0$ are included for 3 consolidation pressures of the $[(0/90)_{10}/0]$ layup and the $[(0_2/90_2)_5/0_2]$ layup consolidated at $p = 0.02$ MPa. (c) Bar chart showing a comparison between measurements and predictions for the $[(0_2/90)_6/0_2]$ and $[(0/90_2)_7/0]$ layups. In all cases, the error bars for the measurements are the standard deviations from Fig. 5.

To perform this comparison, we choose a value of E_1/G_m such that the measurements of $\bar{\lambda}_1^{\text{dom}}/t_0$ agree with the predictions in Fig. 10b. The comparison between measurements and predictions then allows us to infer the effect of consolidation pressure and ply thickness on E_1/G_m . Increasing consolidation pressure reduces E_1/G_m and thus increases G_m . This can be rationalised by noting that consolidation pressure is expected to reduce voids within the uncured prepregs which will increase the shear modulus. Next to investigate the effect of ply thickness, we consider the $[(0_2/90_2)_5/0_2]$ beam consolidated with a pressure $p = 0.02$ MPa and again plot the measured $\bar{\lambda}_1^{\text{dom}}/t_0$ in Fig. 10b to match the predictions. The comparison shows that E_1/G_m increases with ply thickness, i.e. G_m reduces with increasing ply thickness. This result is less obvious but can be understood by recognising that G_m is not solely set by the matrix stiffness but rather the effective stiffness of the ply comprising both matrix and fibres. Given that we expect shearing of the plies to result in inhomogeneous deformations, the larger number of fibres in thicker plies will allow softer (less constrained) deformation modes, resulting in a reduction in G_m . The material properties K_s and G_m extracted from Figs. 10a and 10b as a function of ply thickness and consolidation pressure are summarised in Table 2.

4.5 Validation of the inferred material properties

The comparisons between measurements and predictions in Figs. 10a and 10b suggest that with a consolidation pressure of 0.02 MPa, $G_m = 2.11$ GPa and $K_s = 25 \text{ MPa mm}^{-1}$ for plies of thickness $\sim 140 \text{ } \mu\text{m}$ but G_m reduces to 1.43 GPa when the ply thickness is doubled to $\sim 280 \text{ } \mu\text{m}$ with K_s unchanged (Table 2). However, in Figs. 10a and 10b, the measurements were fitted to the model and thus there is no independent verification of these properties. Here we provide that independent verification using the laminates with variable ply thicknesses, viz. the $[(0_2/90)_6/0_2]$ and $[(0/90_2)_7/0]$ beams series 5 and 6, respectively in Table 1.

We repeated the calculations on the 3-ply arrangement (Fig. 8a) setting K_s 25 MPa mm⁻¹ but making the following modifications to the calculations reported above:

(i) To model the $[(0_2/90)_6/0_2]$ laminate, we kept t_{90} equal to that used in the calculations reported in Section 4.4 and doubled the 0° ply thickness such that $t_0 = 2t_{90}$. The G_m values of the 90° and 0° plies were taken to be those of the ~140 µm and ~280 µm plies, respectively as listed in Table 2.

(ii) Similarly, to model the $[(0/90_2)_7/0]$ laminate we kept t_0 equal to that used in the calculations reported in Section 4.4 and doubled the 90° ply thickness such that $t_{90} = 2t_0$. Now the G_m values of the 0° and 90° plies were taken to be those of the ~140 µm and ~280 µm plies, respectively as listed in Table 2.

Comparisons between the measurements (error bars corresponding to the standard deviations from Fig. 5) and predictions in the form of a bar chart are included in Fig. 10c. The excellent agreement strongly indicates that we have correctly inferred both G_m and K_s including their dependencies on ply thickness.

Table 2: Summary of G_m extracted from the measurements and predictions in Figs. 10a and 10b for the prepreps comprising IM7 fibres in the 8552 matrix. In all cases, $E_1 = 200$ GPa, $\nu_m = 0.3$ and $K_s = 25$ MPa mm⁻¹

Ply thickness	Consolidation pressure p (MPa)	G_m (GPa)
140 µm	0.02	2.11
	0.1	2.50
	0.5	3.07
280 µm	0.02	1.43

5. Concluding remarks

Draping of prepreps (pre-impregnated composite fibres containing a resin) into complex three-dimensional shapes is an important route for manufacturing composite structures. Defects generated during forming can become locked into the structure upon curing, leading to deterioration of the mechanical performance of the composite. Using a combination of in situ X-ray tomography and digital volume correlation, we uncover a previously unreported mode by which fibre waviness develops during bending of prepreg laminates.

Periodic, short-wavelength waviness was observed to form in plies aligned with the longitudinal axis of beams subjected to bending. The waviness develops out of the plane of bending and is therefore not captured by surface-based observations, including three-dimensional digital image correlation. Detailed measurements reveal that the wavelength depends on the laminate topology, including ply arrangement and thickness, as well as beam geometry and processing conditions such as consolidation pressure. A simple numerical model was developed to elucidate the deformation mechanism responsible for waviness formation. Critically, the simulations establish a relationship between the wavelength of the waviness and the material properties of the uncured laminate.

An unexpected consequence of this relationship is that the tomographic measurements, when combined with the numerical simulations, provide a means of extracting material properties of the soft prepreps that are otherwise difficult to measure directly. Together, these results establish a new approach for investigating defect formation during the forming of composite

structures while simultaneously providing access to material properties required for predictive process modelling.

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Supplementary Information

Tomographic observation and modelling of three-dimensional fibre waviness in prepreg draping

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Supplementary S1: Overview of experimental methods

S1.1 Materials and specimen preparation

We used uncured IM7/8552 and AS4/8552 carbon/epoxy unidirectional preregs to prepare the specimens (Table 1) used in this study. The preregs were supplied by Hexcel (<https://www.hexcel.com>) as strips pre-cut to the length of $l = 50$ mm widths w ranging from 2 mm to 8 mm (see Table 1 for the range of widths). Prior to specimen preparation, the preregs were stored at -20°C to prevent curing. Our independent tests showed no significant change in mechanical response for handling times at room temperature of up to approximately 7 hours. We assembled beam specimens at room temperature by hand lay-up using a custom-built consolidation fixture (Fig. S4). The fixture contains a recessed channel that positions the prepreg strips and defines the specimen width and length. Consolidation pressure normal to the laminate using calibrated weights. Plies were added sequentially according to the prescribed stacking sequence. After placing each ply, the consolidation load was applied and maintained for 20 s before adding the next ply. After completion of the lay-up, the same load was applied for 40 min to complete consolidation. Using this procedure, we assembled beam configurations listed in Table 1. The nominal thickness of an individual prepreg ply was $t_{\text{ply}} \sim 140$ μm independent of fibre direction, which gives total laminate thicknesses $h \sim 3$ mm depending on the stacking sequence and the consolidation pressure.

S1.2 In-situ XCT bending experiments

In-situ X-ray computed tomography (XCT) was used to obtain volumetric images of the specimens during bending. All scans were performed using a Nikon XTH 225 ST laboratory XCT system equipped with a Tungsten source (target) and 2000 pixel $\times$ 2000 pixel CCD detector with a resolution of 150 μm pixel $^{-1}$. Geometric magnification achieved via the location of the detector and sample stage relative to the X-ray source enhanced the resolution of the scans to 9.5 μm , which is sufficient to resolve ply-level features. The X-ray beam operated at approximately 100 kV with an exposure time of about 1.4 s, resulting in a scan duration of 100 min for 3142 projections. The captured projections were used to reconstruct 3D volumes, which were exported as uncompressed .raw files and imported into VGStudioMax 2025.2 for post-processing analysis.

Two types of in-situ loading experiments were conducted: (i) the 4-point bending experiments described in the main text and (ii) 3-point bending experiments. Here we briefly describe the setup for the 3-point bending experiment. The in-situ XCT setup and reconstructions of the undeformed and final deformed beams are shown in Fig. S5a. The rollers were displaced in four increments of 3 mm to a final displacement of 12 mm, with tomographic scans performed before loading and after each increment. Unlike the 4-point bending setup, the transparent side plates in the 3-point bending rig were designed with a curved geometry. This allowed the lateral surfaces of the beam to remain visible for digital image correlation (DIC) measurements used to validate the digital volume correlation (DVC) results.

S1.3 High-resolution tomography for microstructural observations

We performed high-resolution X-ray computed tomography (XCT) scans to observe fibre-level microstructural features. We conducted the scans using a Zeiss Xradia Versa 510 laboratory micro-XCT system. The system was operated at 80 kV and 74 μA with an exposure time of 3 s per projection. A total of 1993 projections were acquired using a detector with dimensions of 1988 pixel $\times$ 2030 pixel. The combination of geometric magnification which is achieved through the relative positions of the source, sample and detector and an optical magnification

of $4\times$ resulted in a reconstructed voxel size of approximately $1.95\text{ }\mu\text{m}$. This resolution is sufficient to observe the $5\text{ }\mu\text{m}$ diameter IM7 fibres in $4\times 4\times 4\text{ mm}^3$ imaged volumes. We scanned and reconstructed both undeformed and deformed specimens into 16-bit volumetric datasets for post-processing analysis and comparison of fibre morphology before and after deformation.

S1.4 Digital Image Correlation (DIC)

We performed 3D digital image correlation (3D DIC) [1] to measure the surface displacement and strain fields during the bending experiments and used these measurements as part of the DVC validation protocol. All measurements were performed using a Correlated Solutions stereo DIC system with two optical cameras and an external continuous light source. We first lightly polished the specimen surface to level the plies and then sprayed a stochastic black speckle pattern over a thin white paint background to provide sufficient grayscale contrast for image correlation. Despite the transparency of the Perspex bending setup (Fig. 1b), the part of the beam that goes behind these parts is undetectable with the DIC optical cameras. Therefore, we only used the central region of the beam that can be viewed un-impaired for DIC measurements. We modified the loading setup in the 3-point bending experiment discussed in Supplementary S4. The 3-point bending setup provides a clearer region for DIC (Fig. S5). Photographs of the stereo-DIC setup, the 4-point bending test setup, and the stochastic black pattern on the specimen surface used for image correlation are included in Fig. S6. The 3D DIC measurements provide displacements u_i ($i = 1,2,3$) of material points on the surface of the beam parallel to the $X_1 - X_2$ plane. From these measurements, we can calculate the corresponding surface Green-Lagrange strain e_{ij} .

Supplementary S2: Details of the Digital Volume Correlation (DVC) method

S2.1 Overview of digital volume correlation (DVC)

We performed digital volume correlation (DVC) to measure the 3D displacement fields between the reconstructed XCT volumes at different stages of deformation. The general principle of DVC is similar to digital image correlation (DIC), except that in the DVC method the inherent speckle within a 3D volume is tracked instead of a surface speckle pattern. In the uncured composite specimens in our study, the inherent spatial variation in X-ray attenuation of different microstructural features such as fibres, resin and voids provides the internal speckles required for volume correlation.

We formulated DVC as a non-rigid image registration problem and performed the registration using *Elastix*². Readers are referred to [2–4] for further details and here we provide a brief summary with the key emphasis on the validations performed to ensure the accuracy of the protocol for composite materials. Consider 3D reconstructed grayscale images comprising 3D array of pixels obtained. Let $I_R(X_i)$ denote the reference image, i.e. image of the specimen in the undeformed state and $I_t(x_i)$ the deformation at time t where X_i and x_i denote the location of the same material point in the undeformed and deformed states, respectively. The goal of DVC or non-rigid registration is to relate a material point in the deformed image I_t to the reference image I_R . This implies finding the optimal transformation $\mathbf{T}: (x_i) \mapsto (X_i)$ which maps any point in $I_t(x_i)$ to the corresponding point in the reference image $I_R(X_i)$ where the transformation is given in terms of the displacement field $u_i(X_i)$ as $\mathbf{T}(X_i) = x_i \equiv X_i + u_i(X_i)$.

² Readers are referred to the *Elastix* manual for the details of the registration process and DVC hyperparameters: <https://github.com/SuperElastix/elastix/releases/download/5.3.0/elastix-5.3.0-manual.pdf>

This transformation then spatially aligns I_t to I_R . We use the *Elastix* framework where the transformation is parameterized and described by a set of parameters μ . We therefore denote the transformation as $\mathbf{T}_\mu$ and the optimal transformation is then

$$\hat{\mathbf{T}}_\mu = \underset{\mathbf{T}_\mu}{\operatorname{argmin}} \mathcal{C}(\mathbf{T}_\mu, I_R, I_t), \quad (\text{S2.1})$$

where the cost function, $\mathcal{C}$, is a similarity metric that quantifies the “distance” between transformed images I_R and I_t .

The optimisation problem (S2.1) requires the use of a number of parameters, which we refer to as *hyperparameters*. Readers are referred to the *Elastix* manual for a detailed description and a full list of *hyperparameters*. The hyperparameters that have a major role in the registration problem are:

- (a) *B-spline control-point spacing*: We use a non-rigid free-form deformation (FFD) based on 3D B-splines to parameterise the transformation. The B-spline field is a set of regular grid control points and a set of weights corresponding to these points. The spacing between these control points is an important input parameter and determines the spatial length scale of variations in the displacement field. A smaller control point spacing allows more local variations in displacement to be captured but also increases the number of unknown parameters and can lead to overfitting. A larger spacing gives a smoother displacement field but may miss small-scale deformation features. Our trials, which we discuss subsequently, show that control points of 25 voxels were appropriate for the composite beam problem we have investigated.
- (b) *Similarity metric*: The similarity metric defines the cost function $\mathcal{C}$ and measures how well the transformed deformed volume aligns with the reference volume. Readers are referred to *Elastix* manual for details on mathematical definitions of different choices of similarity metrics. The most naïve approach is the comparison of grayscale values at the voxel level and is called *Mean Squared Difference* in *Elastix*. This similarity metric is appropriate if the gray scale value distributions in I_R and I_t are identical. However, we expect, at least, minor differences in the grayscale value distribution of experimentally obtained scans. The *Advanced Normalized Correlation (ANC)* or *Normalized Mutual Information* similarity metrics in *Elastix* are more appropriate in this context. The former presumes the existence of a *linear* correlation between the grayscale values of I_R and I_t while the latter only presumes the existence of a correlation between the grayscale values. Our verification, that we discuss subsequently, shows that ANC is adequate.
- (c) *Optimiser*: The optimiser updates the transformation parameters μ (in this case the weights of the B-spline control points) to minimise the cost function. *Elastix* has many standard functions to solve the optimisation problem. We found that *Adaptive Stochastic Gradient Descent (ASGD)* and *Quasi-Newton LBFGS* are good choices for the problem investigated here.
- (d) *Image pyramids*: The displacement fields have features of different length scales. If large global-length scale features are present in the displacement field, it can be hard to ascertain local length-scale features directly via a single step optimisation (S2.1). *Elastix* solves this problem by using a hierarchical approach involving down-sampling and blurring. The idea is to solve the image registration problem in multiple steps, where in the initial steps the image is blurred to eliminate local length-scale information and the solver used in (S2.1) is biased to determine the B-spline weights to fit global length-scale features in the displacement field. These optimum weights form the starting point for the fitting that happens at the next step, where the “severity” of down-

sampling and blurring is reduced. We will call each down-sampling and blurring step as an “image pyramid” and the hyperparameters are the number of image pyramids n_{pyr} and the “severity” of the down-sampling and blurring to be carried out in each image pyramid. We converged on $n_{\text{pyr}} = 8$ with a down-sampling ratio 2^{i-1} from the original image to the image used in image pyramid i .

- (e) *Number of iterations*: The maximum number of iterations determines how many optimisation steps are allowed at each image pyramid level. Too few iterations may prevent convergence, while too many increase computational cost. We converged on 500 iterations as an appropriate compromise.

The hyperparameters selected for this study are listed in Table S1.

S2.2 Experimental DVC procedure

The experimental DVC analysis was carried out in two stages. First, rigid registration was used to remove rigid-body motion introduced by repositioning the loading setup between measurements. The resulting registered XCT volumes were then used for non-rigid DVC. In addition, because the specimen undergoes relatively large deformation over the complete loading, the non-rigid DVC was performed incrementally between consecutive loading states and the resulting displacement increments were combined using a multi-stage DVC procedure. We proceed to describe these two steps.

S2.2.1 Rigid registration

Although the imposed roller displacement was fixed while the XCT and DIC measurements were acquired, repositioning the loading setup between measurements caused small shifts in the position of the setup. These motions do not correspond to deformation of the beam and therefore need to be removed before performing non-rigid DVC. We employed the rigid registration tool in *Elastix* to perform the alignment of the beam between consecutive loading steps. Unlike the free-form DVC transformation described in Supplementary S2.1, the rigid transformation is described by only six parameters corresponding to three translations and three rotations.

Importantly, we did not use the beam itself to determine this rigid transformation since the beam deforms between successive loading states. Instead, the rigid registration was performed using a fixed region of the loading setup comprising only one roller. This roller has a small flat surface which breaks its cylindrical symmetry and provides a geometric feature for determining the rigid-body transformation. A mask was used such that only this roller contributed to the rigid registration. The resulting transformation was then applied to the volume to align the loading setup between successive scans. This step ensures that translations or rotations associated with repositioning of the setup do not contribute to the displacement field inferred by DVC. Following rigid registration, the roller region was excluded from the DVC analysis and non-rigid registration was performed on the composite beam using the procedure described in Supplementary S2.1.

S2.2.2 Multi-stage DVC

The deformation over the full bending experiment is sufficiently large that directly correlating the undeformed XCT volume with a volume at the final loading state can fail in the registration process. We therefore performed DVC between consecutive volumes with relatively small increments. The incremental displacement fields were subsequently combined to obtain the displacement of material points relative to the undeformed configuration. After each loading increment, the position of each material point was updated using the displacement obtained

from that DVC step. For the next increment, the displacement field was then evaluated at this updated position, and the new displacement increment was added to the accumulated displacement of the material point. This procedure was repeated through all loading states and allowed the same material points to be tracked throughout the deformation. This multi-stage procedure allows the large deformation of the beam to be reconstructed from a sequence of smaller correlated deformation increments. For more information on the multi-stage DVC process, readers are referred to the Supplementary of [5].

S2.3 Assessment of DVC hyperparameters

The accuracy of the displacement field inferred by DVC depends on both the quality of the XCT volumes and the choice of DVC *hyperparameters*. In the experiments, we increased the beam current, exposure time and number of projections to obtain XCT volumes with sufficient quality for correlation. Here, we focus on the accuracy of the DVC procedure and identify suitable *hyperparameters* for the XCT volumes used in this study.

The optimisation of the DVC hyperparameters was performed in 2 steps: (i) using a synthetic displacement field and (ii) then cross-checked with surface DIC measurements. We proceed to discuss both these steps.

S2.3.1 Synthetic displacement to optimise hyperparameters

Before we proceed to use DVC on experimentally deformed specimens where the resulting displacement field is unknown, we need to investigate the influence the DVC algorithms and hyperparameters have on the inferred displacement fields. We do this by synthetically deforming an X-ray tomographic image I_R of a specimen akin to the specimen of interest to generate an image I_t . The synthetically deformed image I_t is then compared with I_R to determine the transformation $\hat{\mathbf{T}}_\mu$ and thereby the displacement field. This method allows us to tune hyperparameters and investigate the limitations of the DVC method using X-ray data as close as possible to the problem of interest.

The 3D X-ray image I_R is a function such that given a spatial location X_i , I_R outputs the grayscale value at that location. In reality, the X-ray image is a voxelised volume where the values are known at the voxel centroids. The grayscale value at an arbitrary location X_i is obtained by a trilinear interpolation from this 3D grid of voxel centroids. This reference image I_R is then transformed to a new image I'_t as follows. The voxel centroids X_i^c are displaced by a free-form displacement field $u_i^{\text{syn}}(X_i^c)$ such that in the transformed image the centroids are located at $x_i^c = X_i^c + u_i(X_i^c)$. The grayscale value at an arbitrary spatial location x_i in I'_t can then be estimated by again using a trilinear interpolation using the new grid x_i^c of known grayscale values. However, to replicate an experimentally obtained deformed image, both the reference and deformed voxelised volumes need to have the same regular spacing of voxel centroids. We therefore create a new image I_t with a grid of voxel centroids having the same spacing as in I_R . The grayscale values at these centroids of I_t are obtained from the trilinear interpolation of grayscale values at the centroids in I'_t .

We generated a synthetically deformed volume I_t from an experimentally acquired reference XCT volume I_R by imposing a displacement field

$$u_1^{\text{syn}} = -0.05X_1X_2; \quad u_2^{\text{syn}} = 0.3X_1^2 \quad \& \quad u_3^{\text{syn}} = 0, \quad (\text{S2.2})$$

that generates a combined bending and shear deformation similar to what is observed in 3-point bending (Fig. S7). We then perform DVC between I_R and I_t to obtain u_i^{DVC} . Since the imposed

displacement field u_i^{syn} is known, we can assess the accuracy of the DVC method by comparing u_i^{DVC} and u_i^{syn} . For each displacement component $i = 1, 2, 3$, we define the normalised error field ξ_i (i.e. error at every material point X_j) as

$$\xi_i(X_j) = \frac{|u_i^{\text{syn}} - u_i^{\text{DVC}}|}{\max(|u_1^{\text{syn}}|, |u_2^{\text{syn}}|, |u_3^{\text{syn}}|)}. \quad (\text{S2.3})$$

This method allows us to compare different choices of DVC *hyperparameters* and determine which choices would provide the minimum error ξ_i .

For the sake of brevity, we only discuss in detail two hyperparameters: (i) choice of similarity metric and (ii) choice of optimiser. First consider the *Advanced Normalized Correlation (ANC)* and *Normalized Mutual Information (NMI)* with all other hyperparameters held fixed at the reference values in Table S1. Distributions of ξ_i are included in Fig. S8 using both the ANC and NMI similarity metrics. NMI has a larger ξ_2 error and therefore we selected the ANC metric in our DVC analysis. Next consider the *Adaptive Stochastic Gradient Descent (ASGD)* and *Quasi-Newton LBFGS* optimisers. Again, distributions of ξ_i are included in Fig. S9 for the ASGD and LBFGS optimisers with all other hyperparameters held fixed at their reference values in Table S1. Clearly the ASGD optimiser gives superior accuracy.

Similar studies we conducted by varying B-spline control point spacings from 15 to 25 voxels, image pyramids n_{pyr} over the range $4 \leq n_{\text{pyr}} \leq 8$ and number of iterations ranging from 500 to 2000. The values of these hyperparameters we converged on are listed in Table S1.

S2.3.2 Experimental validation using DIC measurements

To confirm the fidelity of the calibrated hyperparameters listed in Table S1, we performed an experimental validation. Given that no other volumetric technique exists, our experimental validation involved using the more established digital image correlation (DIC) methods (Supplementary S1.4) to make surface strain measurements and also perform X-ray based DVC measurements on the same specimens. After each loading increment, an XCT scan and a stereo-DIC measurement of the specimen surface were acquired at the same imposed roller displacement. The loading setup was repositioned between these measurements and the rigid registration procedure described in Supplementary S2.1.1 was used to remove the rigid-body motion from the DVC measurements. The surface strains were then extracted from the full volumetric DVC measurements and compared with the DIC measurements.

We report this comparison for both 3- and 4-point bending experiments (Supplementary S1.2). A comparison between the DIC and DVC surface Green-Lagrange strain components e_{12} , e_{11} and e_{22} at the end of the deformation of the 3- and 4-point bending experiments is shown in Fig. S10. The DVC and DIC measurements show similar overall spatial strain distributions. However, DVC additionally resolves smaller-scale deformation features such as inter-ply shear that are not captured by the DIC measurements. This provides an independent verification of the DVC procedure and the hyperparameters identified in Table S1 using the synthetic displacement analysis. We used the selected parameters for the DVC measurements presented in the main text.

Supplementary S3. Analysis of 3-point bending measurements

We carried out additional 3-point bending measurements to investigate whether this instability we report in the main text occurs more broadly under imposed bending deformations. Here we report those measurements

S3.1 Full-field strain measurements in 3-point bending

The in-situ 3-point bending XCT setup and reconstructions of the undeformed and final deformed beams are shown in Fig. S5a. The rollers were displaced in four increments of 3 mm to a final displacement of 12 mm, with tomographic scans performed before loading and after each loading increment.

Full-field strain distributions measured on the $X_1 - X_2$ mid-plane of the beam at the final deformed state are included in Fig. S5b. The normal strain components e_{11} , e_{22} and e_{33} show distributions that are qualitatively similar to those observed in the 4-point bending experiments. The strain e_{22} however, remains relatively small on the roller, which indicates that there is a limited thickness extension in this region. A large shear strain e_{12} is observed (changing sign immediately under the central roller), consistent with the imposed 3-point bending deformation.

The main difference from 4-point bending occurs in the out-of-plane shear strain component e_{23} . The periodic pattern remains visible within the 0° plies, but it is now concentrated in the central region of the beam where the bending moment is maximum, and it does not extend uniformly along the length of the beam. These results show that the higher-order buckling mode associated with the periodic e_{23} still exists under 3-point bending, but its distribution is affected by the distribution of the imposed bending moment.

S3.2 Void growth and strain localisation

To further investigate the effect of shear in 3-point bending, we compare the DVC-inferred volumetric strain field with the void distribution obtained from the XCT reconstructions. To infer the void distribution, consider the grayscale distribution of the imaged undeformed beam (Fig. S11). There are 2 peaks corresponding to material and air. We chose the range marked in Fig. S11 as corresponding to air and used this to mark voids within the beam. The void distribution was then inferred in the undeformed and deformed beams using this definition of voids. Distributions of voids on $X_1 - X_2$ mid-plane of the beam in the undeformed and final deformed states are included in Fig. S12a. In the undeformed state, numerous small voids are distributed throughout the laminate. These voids are relatively small and dispersed between the plies. After deformation, the void distribution changes significantly. Some smaller voids appear to close, while elongated and more continuous voids develop along the interfaces between layers. The formation of these elongated defects indicates that inter-ply shear promotes void coalescence and growth during deformation.

The corresponding distribution of volumetric strain e_{vol} on the same plane is presented in Fig. S12b and Fig. S12c shows an overlay of the void distribution and the volumetric strain field at the final deformed state. Regions of positive e_{vol} align with the elongated voids observed after deformation indicating that local volumetric expansion occurs in the regions where void growth and coalescence happen. We conclude that the volumetric strain field can quantitatively measure void development within the beam.

Supplementary S4. Details of the finite element calculations

S4.1 Constitutive relations and reference material properties

The UD plies are modelled as orthotropic elastic continua with the elastic stress σ_{ij} versus strain ε_{ij} relation given by

$$\begin{bmatrix} \varepsilon_{11} \\ \varepsilon_{22} \\ \varepsilon_{33} \\ \gamma_{23} \\ \gamma_{31} \\ \gamma_{12} \end{bmatrix} = \begin{bmatrix} \frac{1}{E_1} & -\frac{\nu_{21}}{E_2} & -\frac{\nu_{31}}{E_3} & 0 & 0 & 0 \\ -\frac{\nu_{12}}{E_1} & \frac{1}{E_2} & -\frac{\nu_{32}}{E_3} & 0 & 0 & 0 \\ -\frac{\nu_{13}}{E_1} & -\frac{\nu_{23}}{E_2} & \frac{1}{E_3} & 0 & 0 & 0 \\ 0 & 0 & 0 & \frac{1}{G_{23}} & 0 & 0 \\ 0 & 0 & 0 & 0 & \frac{1}{G_{31}} & 0 \\ 0 & 0 & 0 & 0 & 0 & \frac{1}{G_{12}} \end{bmatrix} \begin{bmatrix} \sigma_{11} \\ \sigma_{22} \\ \sigma_{33} \\ \sigma_{23} \\ \sigma_{31} \\ \sigma_{12} \end{bmatrix}, \quad (\text{S4.1})$$

where $X_1 - X_3$ is the plane of the ply and X_1 the fibre direction and $\varepsilon_{ij} = \gamma_{ij}/2$. Symmetry of the compliance matrix implies there are 9 independent elastic constants, which we here take to be the Young's moduli (E_1, E_2, E_3), the shear moduli (G_{23}, G_{31}, G_{12}) and the Poisson ratio's ($\nu_{12}, \nu_{13}, \nu_{23}$). The reference values of these parameters are listed in Table S2. The parameters $E_2 = E_3$ and $G_{23} = G_{31} = G_{12}$ are set to be equal and denoted by E_m and G_m , respectively. The sensitivity of the predictions to these values is investigated in the main text. Similarly, we set $\nu_{12} = \nu_{13} = \nu_{23} = \nu_m = 0.3$ (the predictions are insensitive to the choice of the Poisson's ratio) while the modulus E_1 is set by the fibre stiffness and volume fraction and hence taken from the material datasheet provided by Hexcel ® and listed in Table S2.

The interfaces between plies are represented by cohesive layers that transfer normal and shear tractions with a linear decoupled traction-separation law. Using the co-ordinate system where $X_1 - X_3$ is the plane of the ply and X_1 the fibre direction, the traction t_i versus relative displacement δ_i between the plies is given by

$$\begin{Bmatrix} t_1 \\ t_2 \\ t_3 \end{Bmatrix} = \begin{bmatrix} K_s & 0 & 0 \\ 0 & K_n & 0 \\ 0 & 0 & K_s \end{bmatrix} \begin{Bmatrix} \delta_1 \\ \delta_2 \\ \delta_3 \end{Bmatrix}. \quad (\text{S4.2})$$

Here K_s is a shear stiffness, while K_n is the normal stiffness. In our experiments, there is no normal separation of the plies, so we set $K_n \gg K_s$ with K_n set to a high value listed in Table S2. On the other hand, K_s is varied in the paper with the value listed in Table S2 as the reference value. Note that this value $K_s = 100 \text{ GPa m}^{-1}$ differs from the $K_s = 25 \text{ GPa m}^{-1}$ value we infer from measurements as appropriate to model the uncured prepregs; see Table 2.

S4.2 Buckling onset force

The FE calculations in Fig. 8b show the onset of an instability. The predicted deformation at the onset of the instability was then used to calculate the wavelengths λ_1 and λ_2 . The force F_c

at the onset of the instability is of less interest, but here we show these predicted forces for the sake of completeness. Predictions of the normalised buckling onset force $\bar{F}_c \equiv F_c/(3wt_0E_1)$, are shown in Fig. S3 as a function of $K_s w/E_1$ for selected choices of E_1/E_m , E_1/G_m and w/t_0 . The force is relatively insensitive to all these parameters except E_1/G_m : $\bar{F}_c$ increases with decreasing E_1/G_m suggesting that increasing shear modulus G_m delays the onset of the instability.

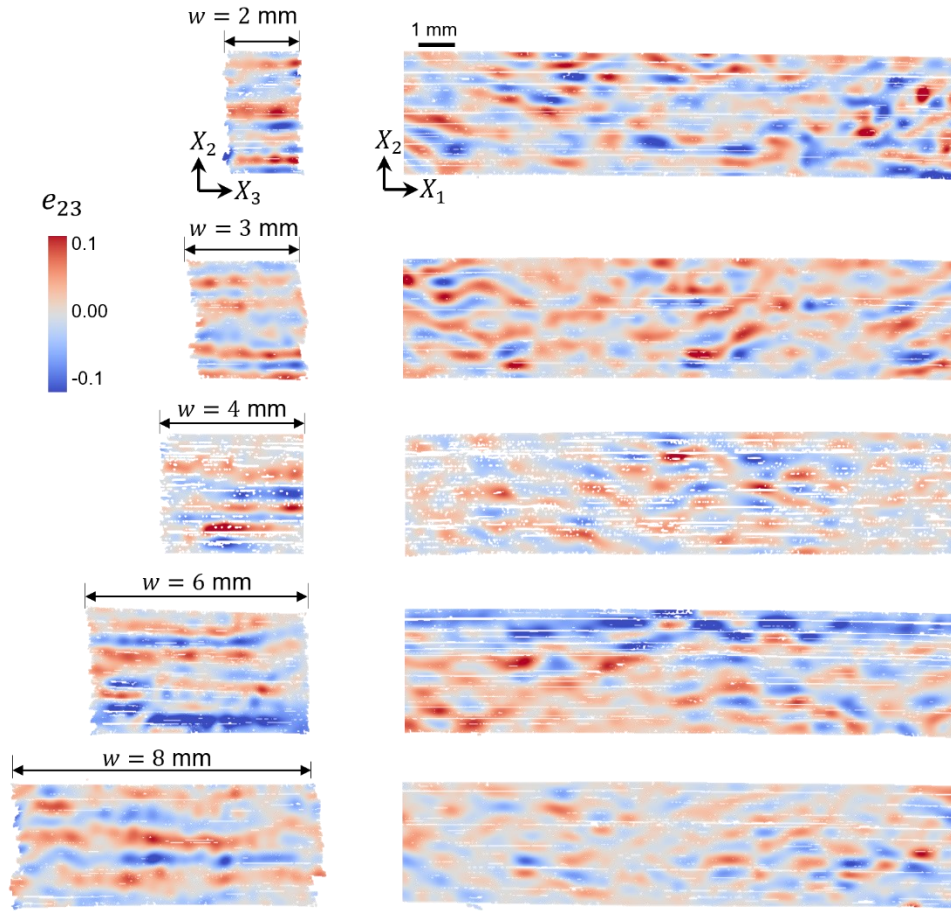


Figure S1: Measurements of the distributions of the strain field e_{23} on central orthogonal $X_1 - X_2$ and $X_2 - X_3$ planes at the final deformed state of the $[(0/90)_{10}/0]$ beams of width $w = 2, 3, 4, 6$ and 8 mm subjected to 4-point bending. All beams were consolidated with $p = 0.02$ MPa and made from plies with the IM7 fibre. The observations are shown on the undeformed configuration.

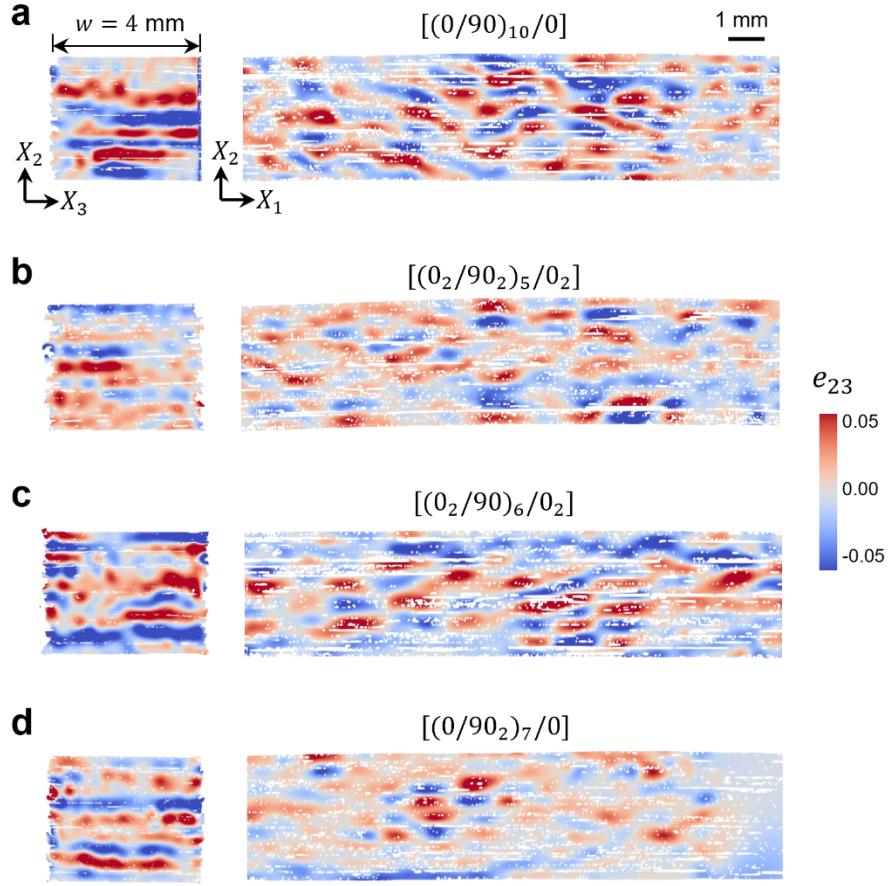


Figure S2: Measurements of the distributions of the strain field e_{23} on central orthogonal $X_1 - X_2$ and $X_2 - X_3$ planes at the final deformed state of the (a) $[(0/90)_{10}/0]$, (b) $[(0_2/90_2)_5/0_2]$, (c) $[(0_2/90)_6/0_2]$, and (d) $[(0/90_2)_7/0]$ beams subjected to 4-point bending. All beams were consolidated with $p = 0.02$ MPa and made from plies with the IM7 fibre and width $w = 4$ mm. The observations are shown on the undeformed configuration.

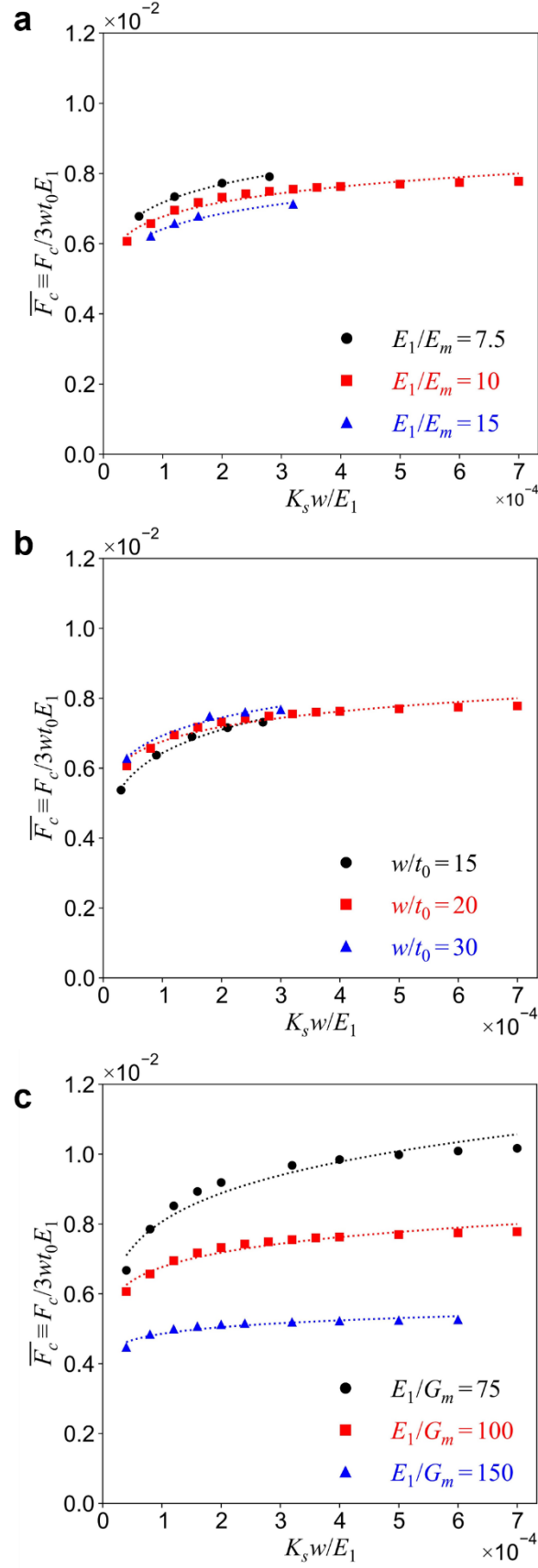


Figure S3: Predicted normalised force $\bar{F}_c$ as a function of $K_s w / E_1$ for choices of (a) E_1/E_m , (b) w/t_0 and (c) E_1/G_m . All other material parameters are fixed at their reference values (Table S2).

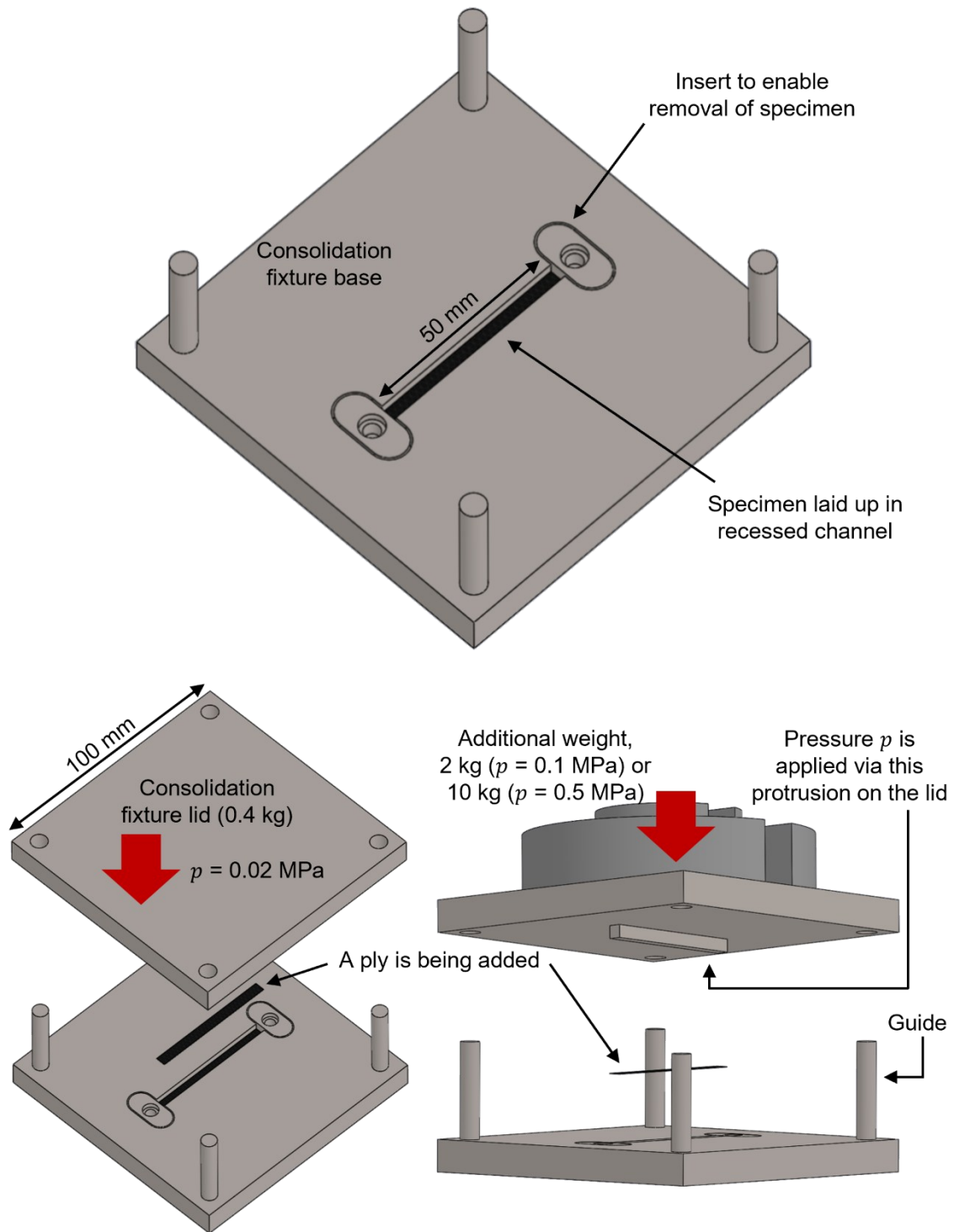


Figure S4: Schematic of the custom-built fixture to enable the layup of the beams and apply a controlled consolidation pressure p by placing a weight as shown.

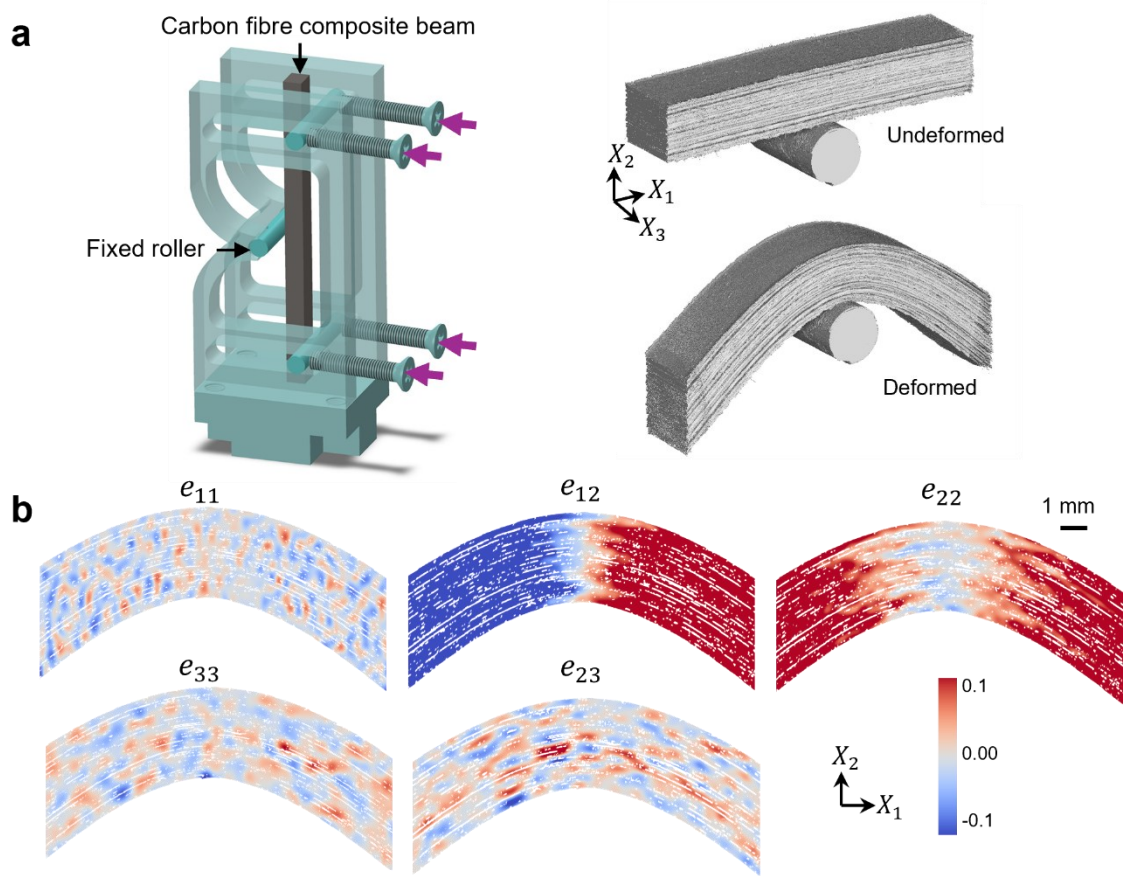


Figure S5: (a) Sketch of the in situ XCT test setup for 3-point bending and corresponding XCT reconstructions of the undeformed and deformed reference $[(0/90)_{10}/0]$ beam. (b) Measured distributions of selected components of the Green-Lagrange strain over the $X_1 - X_2$ mid-plane of the beam deformed to its final state with a 12 mm roller displacement. The strains are shown on the deformed configuration of the beam.

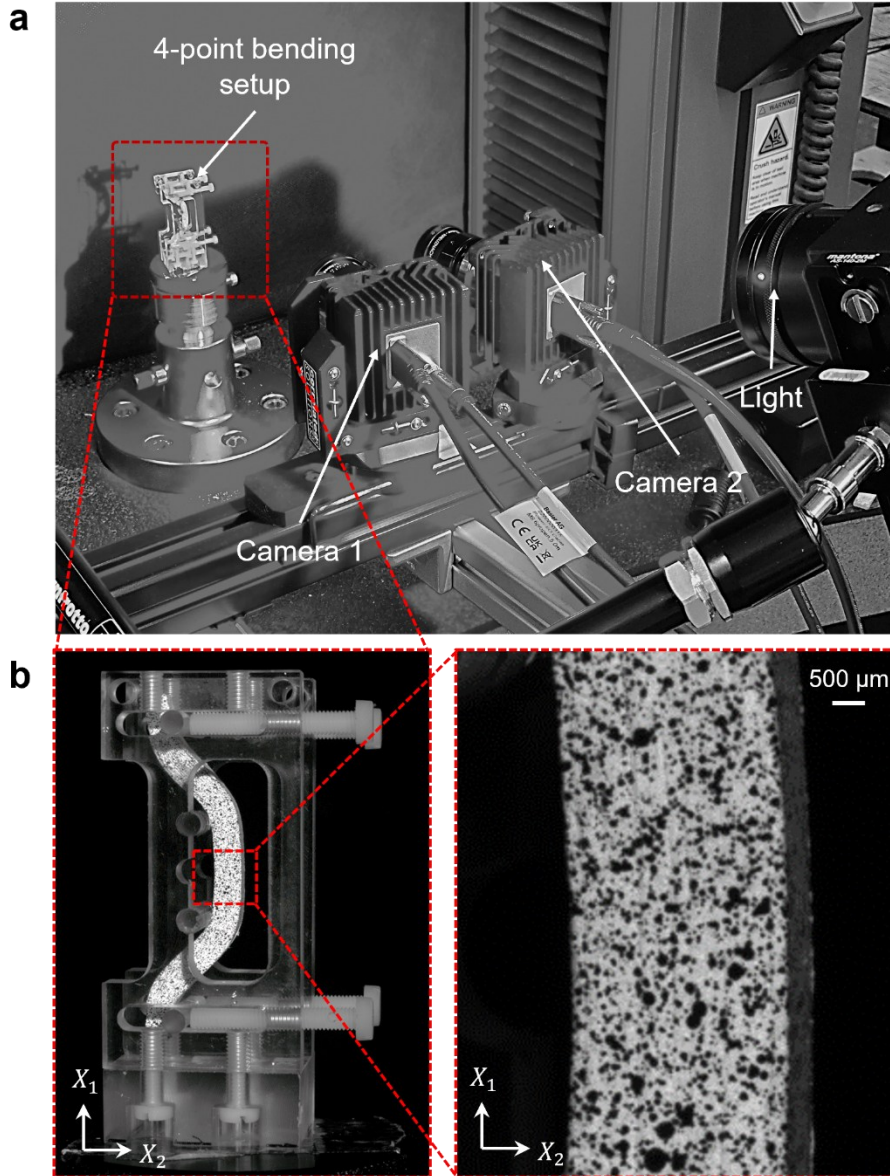


Figure S6: Experimental setup for 3D digital image correlation (DIC). (a) Stereo-DIC system including two cameras, a light source, the bending setup (4-point in this case) and the specimen. (b) 4-point bending setup and the central region of the specimen accessible to the DIC cameras. The dashed box indicates the magnified view of the specimen surface showing the stochastic black speckle pattern on a white background used for image correlation.

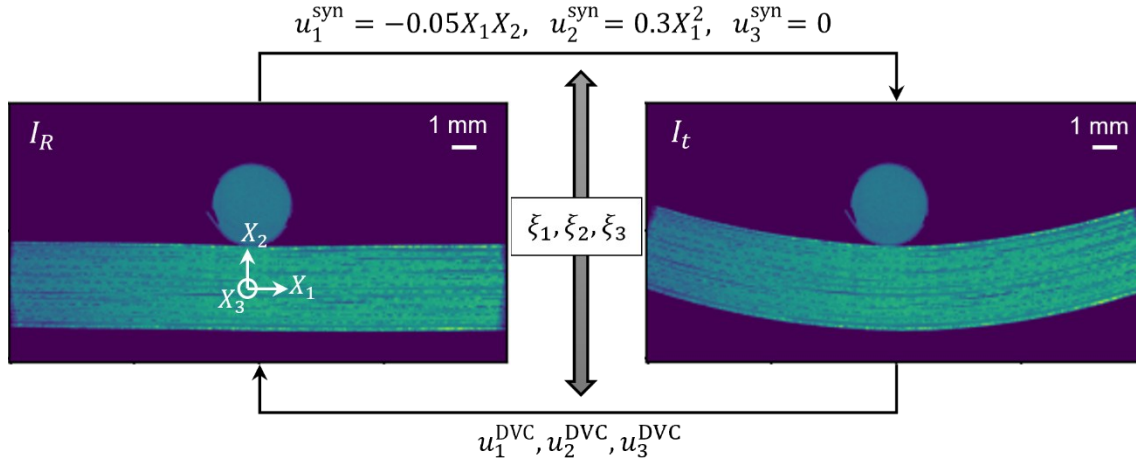


Figure S7: Procedure to assess the DVC hyperparameters using synthetic displacements. An experimentally scanned reference XCT volume I_R is deformed by a prescribed displacement u_i^{syn} to generate a synthetic deformed volume I_t . Then, DVC is performed between the two volumes to obtain u_i^{DVC} and is compared with the prescribed displacement field u_i^{syn} .

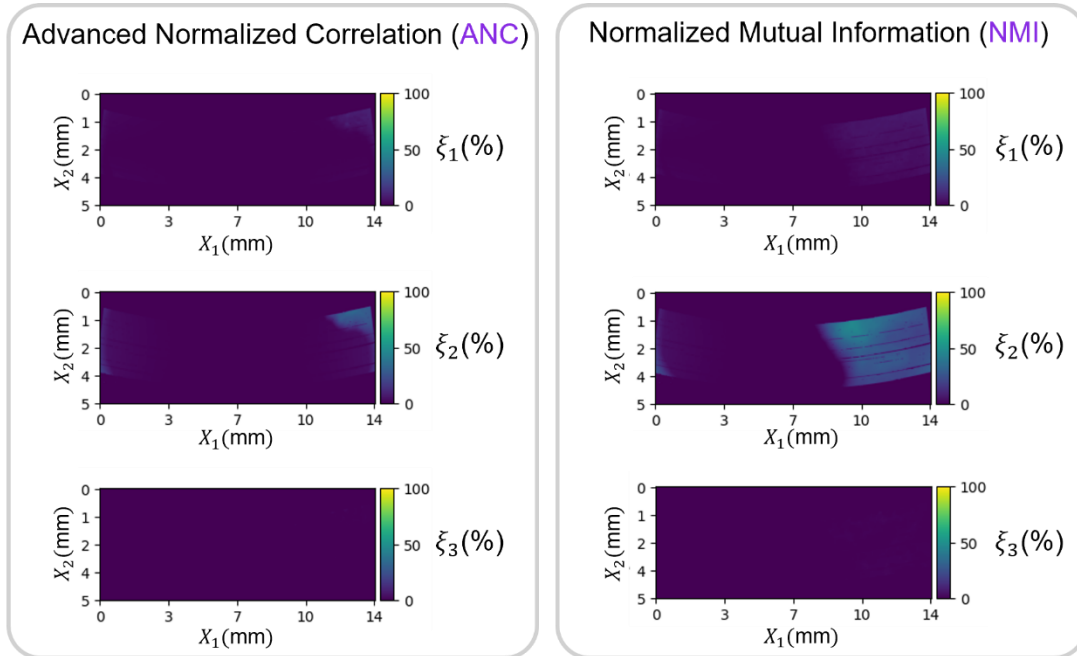


Figure S8: Comparison of the normalised displacement errors ξ_1 , ξ_2 and ξ_3 obtained using the Advanced Normalized Correlation (ANC) and Normalized Mutual Information (NMI) similarity metrics. All other hyperparameters are fixed at their reference values listed in Table S1.

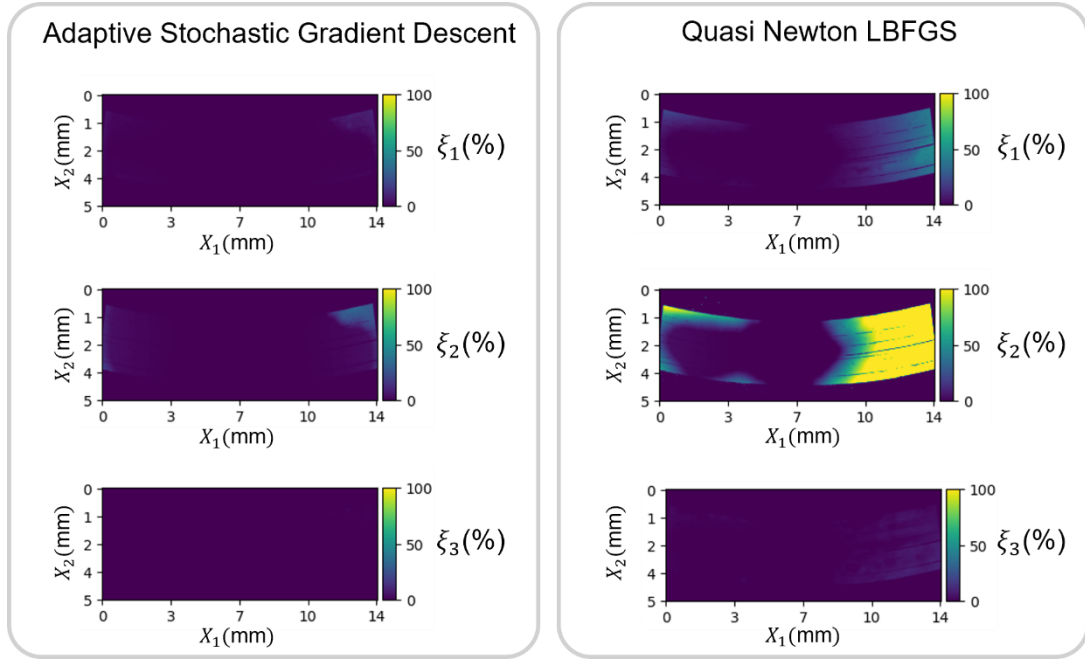


Figure S9: Comparison of the normalised displacement errors ξ_1 , ξ_2 and ξ_3 obtained using the Adaptive Stochastic Gradient Descent and Quasi-Newton LBFGS optimisers. All other hyperparameters are fixed at their reference values listed in Table S1.

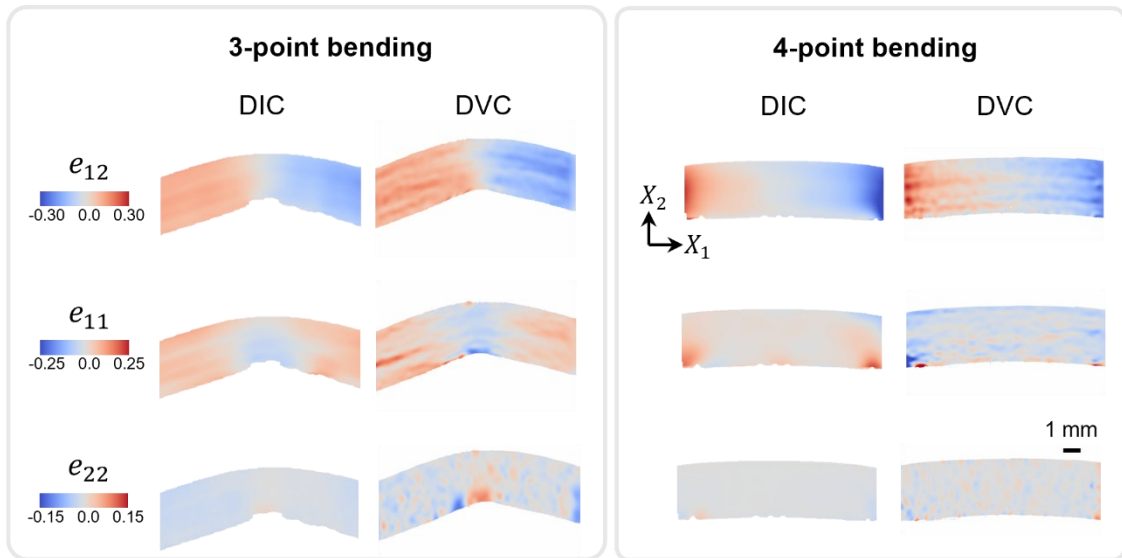


Figure S10: Comparison of surface Green-Lagrange strain fields e_{12} , e_{11} and e_{22} obtained using DIC and DVC for the 3- and 4-point bending experiments at the final deformed states.

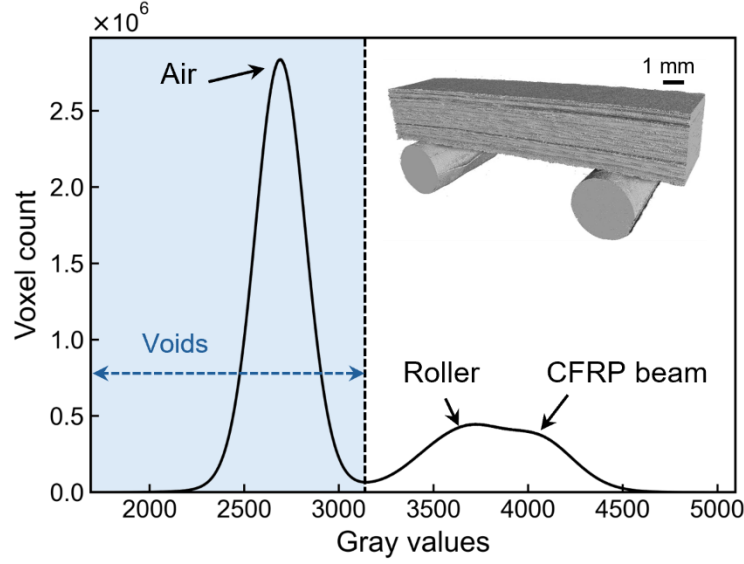


Figure S11: The gray value histogram of the undeformed reference $[(0/90)_{10}/0]$ beam. The two peaks corresponding to material and air are marked and the range of gray values used to denote voids marked.

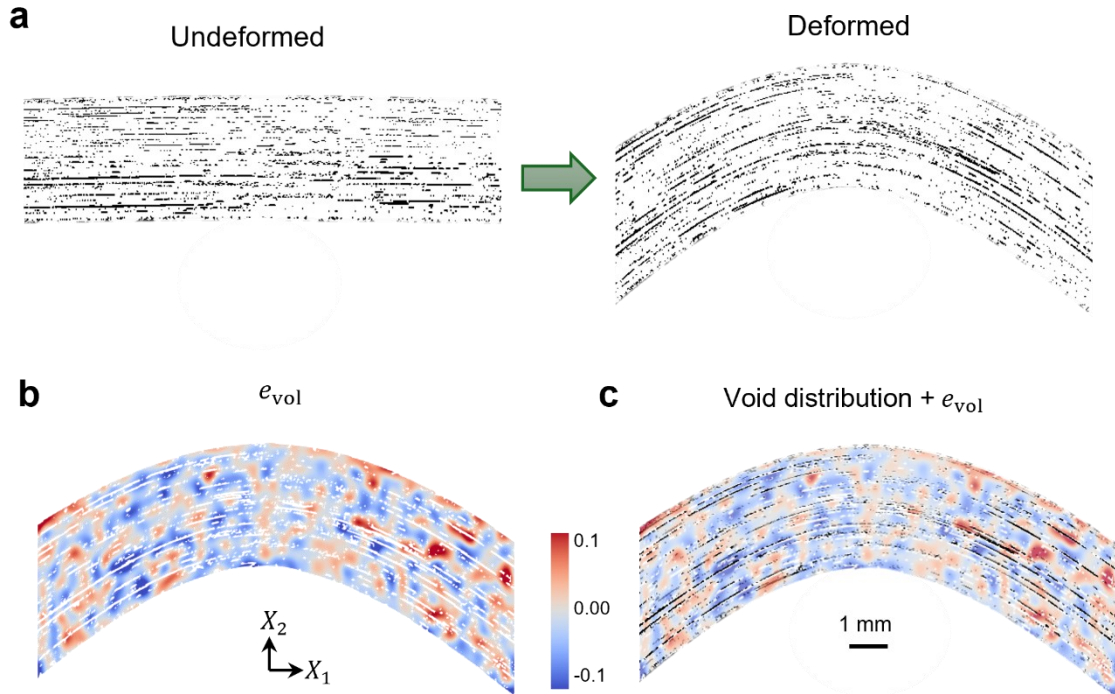


Figure S12: (a) Void distributions on the beam subjected to 3-point bending in the undeformed and final deformed states. Voids are shown in black. (b) Volumetric strain e_{vol} at the final deformed state. (c) Overlay of the void distribution and e_{vol} showing their spatial correspondence.

Supplementary References

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Table S1: Summary of the reference DVC hyperparameters selected from the synthetic displacement tests.

Hyperparameter	Value
Optimizer	Adaptive Stochastic Gradient Descent (ASGD)
Similarity metric	Advanced Normalised Correlation (ANC)
Number of iterations	500
Image pyramid levels n_{pyr}	8
B-spline control Point Spacing (Voxel)	25

Table S2: The reference material parameters used in the FE calculations

Ply properties	
E_1	200 GPa
$E_m = E_2 = E_3$	20 GPa
$G_m = G_{23} = G_{31} = G_{12}$	2 GPa
$\nu_m = \nu_{12} = \nu_{13} = \nu_{23}$	0.3
Cohesive layer properties	
K_n	5000 GPa m ⁻¹
K_s	100 GPa m ⁻¹