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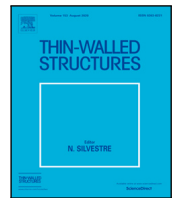
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Full length article

Fracture resistance of tubular composite joints under combined axial and in-plane bending[☆]

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ABSTRACT

Wrapped composite joints offer a promising alternative to welded tubular joints in fatigue-critical offshore structures by reducing stress concentrations, improving fatigue performance, and reducing material consumption. While their effectiveness under debonding-dominated failure modes has been demonstrated and characterized, the composite material cracking dominated failure mechanism remains unexplored. Tubular joints subjected to offshore loading conditions experience multiple loading directions, inducing complex stress states at the root of the composite wrap. This underscores the need to understand the influence of multiaxial loading on the composite's static fracture resistance. This paper presents an experimental and numerical investigation into the static fracture behaviour of tubular composite X90 joints, comprising six specimens subjected to monotonic axial tension, in-plane bending, and combined axial-in-plane bending, with two specimens per loading case. The load–deformation response, damage evolution, and governing failure modes are characterized using full-field strain measurements from three-dimensional digital image correlation and distributed fibre-optic sensing, enabling detailed monitoring of debonding and composite fracture initiation.

The experimental results indicate an approximately linear interaction between the axial force and in-plane bending resistances from the limited set of specimens tested. A finite element modelling framework incorporating anisotropic plasticity, cohesive interface modelling, and damage-based fracture is developed. The model accurately captures the failure mode and ultimate resistance under uniaxial tensile loading. However, under bending-dominated and combined axial-in-plane bending conditions, numerical simulations increasingly overpredict experimental results, highlighting the limitations of modelling the composite material with nonlinear response using existing anisotropic plasticity models.

1. Introduction

Anthropogenic greenhouse gas emissions are accelerating the need to transition towards low-carbon energy systems and the increased deployment of renewable energy worldwide. The configuration of future green energy systems will be governed mainly by technological advancements, innovation rates, and cost-reduction potentials [1]. Among offshore technologies, offshore wind is the most mature and commercially viable option due to its high technological readiness and the availability of strong, stable wind resources, enabling reliable energy generation [2]. As nearshore sites become constrained by spatial, environmental, and socio-economic factors, expansion into deeper waters is

required to exploit vast untapped potential. This shift demands larger and more robust support structures to accommodate increasing turbine capacities, introducing both technical challenges and opportunities for innovation.

Monopile foundations have historically dominated offshore wind installations due to their simplicity, accounting for approximately 80.5% of substructures, while jackets represent around 19% according to WindEurope (2020). However, deeper water deployment necessitates a transition to jacket foundations, which offer improved structural performance and economic efficiency for high-capacity turbines [3,4]. Support structures contribute approximately 22% of total project costs

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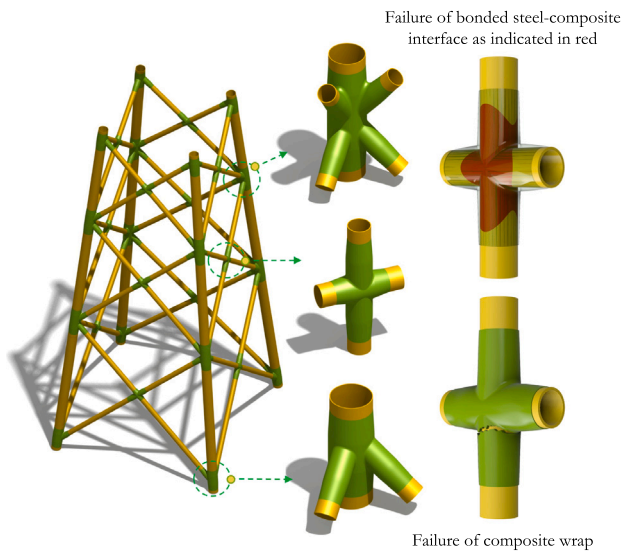


Fig. 1. Offshore wind turbine jacket structure with wrapped composite joints and the two governing failure modes.

[5], with a significant portion attributed to conservative design approaches driven by fatigue-critical behaviour in welded tubular joints. These joints are particularly prone to stress concentrations, especially at weld toes, making them critical components in the structural design.

To maintain sufficient safety and durability by mitigating stress concentrations, previous research focuses on several reinforcement strategies for tubular joints under axial and bending loads, including internal ring stiffeners, doubler plates, collar plates, and composite wraps. Collar plates, externally welded steel stiffening plates affixed to the chord around the brace intersection, enhance ultimate strength and stiffness in T/Y- and X-joints, with their performance characterized under both ambient and fire conditions [6–8]. While internal ring stiffeners and doubler plates are effective, their placement within the joint makes them unsuitable for repairing in-service joints. Collar plates and composite wraps, applied externally to the chord surface, offer greater versatility for retrofit applications. Composite reinforcement [9–11] benefits from the superior mechanical and physical properties, though its effectiveness depends critically on the quality of the steel-composite interface bond. These strategies, however, remain fundamentally retrofit-oriented, mitigating stress concentrations at an existing weld rather than eliminating the weld as the underlying source of the problem.

To address the challenges associated with conventional welded joints, including complex fabrication, high production costs, and extended manufacturing times, a novel solution, termed wrapped composite joints (a.k.a. tubular composite joints), has been proposed in the Netherlands in the past decade. This technology forms the core of a joint industry project led by Delft University of Technology and technology start-up Tree Composites B.V. [12]. In this concept, the circular hollow section (CHS) consisting of brace and chord members is bonded using E-glass fibre-polymer composites combined with a vinyl ester-based thermoset resin, which is geometrically optimized to minimize stress concentrations (Fig. 1). Wrapped composite joints aim to enhance structural performance while enabling rapid assembly, improved cost efficiency, and a reduced overall carbon footprint compared to traditional welded joints. The enhanced performance arises from load transfer through a continuous composite wrap, rather than through localized weld regions that govern the fatigue behaviour of conventional tubular joints.

A demonstration of the proposed technology was conducted through a full-scale fatigue test on an X-shaped wrapped composite joint, designed based on a representative offshore jacket foundation case study.

The reference foundation supports a 14 MW wind turbine located in the North Sea at a water depth of 40 m. The joint geometry employs $\phi 610 \times 25$ mm S355J2H CHS for both brace and chord members, selected to strategically prevent fatigue failure of the steel tubes prior to damage initiation in the composite wrap [13]. The experimental results demonstrated a markedly enhanced fatigue endurance, with the wrapped composite joint exceeding the design fatigue life of an equivalent welded tubular joint by more than 70 times [14].

Depending on the joint design, two primary failure modes may govern the structural response: (i) debonding failure, characterized by degradation of the steel–composite bonded interface governed by a traction–separation law, and (ii) fracture of the composite wrap material, as illustrated in Fig. 1 [15]. Previous research on wrapped composite joints primarily focuses on interface behaviour [13,15–24], with extensive static and fatigue testing conducted on small-, medium-, and large-scale specimens corresponding to 1/12, 1/3.6, and 1/2 of full scale. He and Pavlović [15] highlight that under tensile loading, the joints exhibit a quasi-ductile debonding response, which is attributed to the initiation and propagation of partial debonding along the chord surface, followed by full debonding at the primary bonded interface on the brace, ultimately resulting in pull-out of the steel brace from the composite wrap. In contrast, in-plane bending tests reveal partial debonding and composite cracking as the governing failure mechanisms.

To extend understanding of wrapped composite joint behaviour, it is quintessential to assess their performance under multiaxial loading conditions representative of wind, wave, and tidal actions, and to identify the associated failure modes. A study by [24] investigates a medium-scale (1/3.6 full-scale) X90 joint subjected to monotonic uniaxial tension, uniaxial bending, and combined multiaxial tension-bending. An X90 joint refers to brace members intersecting the chord at an angle of 90° , forming a cross-shaped geometry. The joint consists of $\phi 168 \times 12.5$ mm CHS, specifically selected to prevent steel failure before damage initiation in either the steel–composite interface or the composite material itself. The results indicate that under uniaxial tension, debonding is the sole failure mode. Consistent with [15], debonding initiates at both the chord (secondary bonded interface) and the brace (primary bonded interface), with final failure governed by complete deterioration of the primary bonded interface. Under uniaxial out-of-plane bending, combined brace and chord debonding occurs alongside steel yielding. In addition, composite cracking initiates and propagates near the wrap termination. In-plane bending results indicate lower resistance compared to out-of-plane bending, with composite fracture governing failure, typically localizing at the wrapped brace-chord intersections. Under combined tensile and out-of-plane bending loading, debonding remains the dominant failure mode, similar to uniaxial tension, however, a greater extent of debonding is observed due to the suppression of steel yielding. Overall, this study demonstrates that under bending loading conditions, the wrapped composite joints fully exploit 100% of the plastic bending capacity of the CHS members, highlighting the excellent bending resistance and rotational capacity provided by the composite wrap.

It is evident that the innovative technology demonstrates promising static and fatigue performance when governed by the debonding failure mode. However, it is equally essential to investigate the behaviour of these joints under the alternative failure mode involving fracture of the composite wrap. Addressing this aspect is necessary to achieve a comprehensive understanding of joint behaviour and to enable the development of reliable numerical models, which represent a critical step towards implementation in real-life structures. A key drive for this research also involves building the ability to have optimized thickness of composite at full-scale.

This paper aims to improve the understanding of the composite fracture failure mode in wrapped composite joints. To this end, experiments are conducted on a medium-scale X90 joint configuration subjected to monotonic uniaxial tension, uniaxial in-plane bending, and biaxial

tension combined with in-plane bending. Based on the experimental campaign, the interaction governing the combined loading response are identified and represented as a failure envelope. In addition, advanced finite element analyses are performed by extending modelling strategies previously developed and calibrated for debonding-dominated failure. The primary numerical challenge arises from the complex geometry of the composite wrap, characterized by variable thickness, double curvature, intricate ply drops, and the stacking sequences, which altogether induce highly complex stress states within the fibre-polymer composite material [23]. At this stage of development, it is therefore crucial to assess the accuracy with which these models predict composite fracture failure in wrapped composite joints.

2. Ultimate load experiments

This experimental campaign investigates the ultimate load behaviour of wrapped composite joints using six identical medium-scale X90 specimens (1/3.6 of full scale). All tests are conducted in the Stevin II Laboratory of the Department of Engineering Structures, Faculty of Civil Engineering and Geosciences, Delft University of Technology. The joints are subjected to three distinct loading conditions, with two specimens tested for each loading case. Two different test set-ups are employed in the campaign, as described in Section 2.2.

2.1. Specimen preparation

The specimen detailing of the medium-scale X90 joint designed for this experimental campaign is shown in Fig. 2. The principal connection design is selected to extend the research reported in [24], with a crucial modification where the composite wrap thickness is reduced to increase stress levels within the composite and promote fracture before full debonding. For specimen fabrication, the CHS steel tubes are first grit-blasted and then chemically degreased using acetone before lamination, ensuring adequate interfacial bonding between the steel substrate and the composite wrap. No discrete adhesive layer is employed; instead, the composite laminate is applied directly onto the steel surface, thereby eliminating the additional failure mechanism that could occur in a thick layer of adhesive as a failure of the bulk material referred to as cohesive failure in the terminology of adhesively bonded joints. The composite wrap consists of a bi-directional plain-weave E-glass fibre reinforcement embedded in a vinyl ester-based thermoset resin system, forming a predominantly quasi-isotropic laminate. The laminate exhibits a nominal thickness of 8 mm in the cylindrical regions, increasing to 12 mm at the joint root and tapering to 0 mm at the wrapping end. The specimens are manufactured using a hand-layup process in sequential stages under factory-controlled conditions at ambient temperature. The staged laminate build-up enables achieving the desired thickness transitions and controlled ply drops.

To ensure manufacturing consistency and high laminate quality, excess resin and entrapped air are systematically removed during the lamination process. Following fabrication, the joints are post-cured at 80 °C for 3 h. Fig. 3 illustrates the X90 joint with initially aligned brace and chord members, together with the configuration after completion of the composite lamination. Before wrapping, the brace and chord members are temporarily aligned using a structural adhesive. To facilitate mounting of the specimens on the test bench, machined steel flanges are welded to the brace ends using full-penetration butt welds. The weld regions are subsequently ground smooth on both the inner and outer surfaces. The flanges incorporate bolt holes, enabling the specimens to be fixed to the test equipment using M20 bolts.

2.2. Experimental set-up

Monotonic tests are conducted using two different experimental setups, primarily for two reasons: the nature of the applied loading and

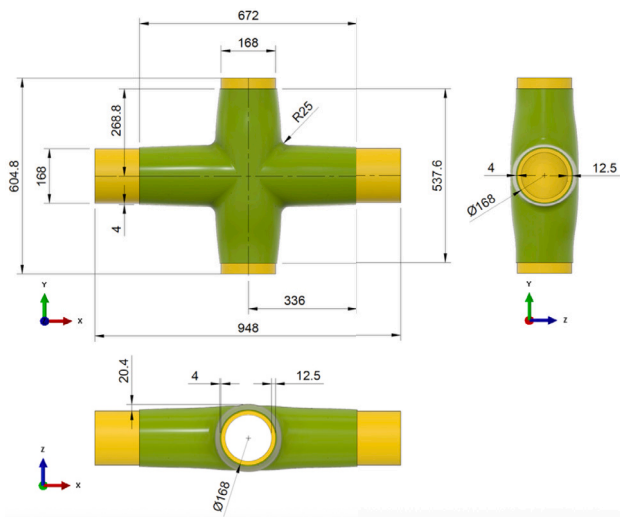


Fig. 2. Geometry of the medium-scale X90 joint designed for composite fracture failure. All dimensions are given in mm.



Fig. 3. X90 joint during manufacturing: before lamination (left) and after composite lamination (right).

the required load capacity. Specimens subjected to combined axial and bending loads require specialized testing equipment capable of independently controlling multiple degrees of freedom. For this purpose, a six-degree-of-freedom hydraulic testing system, referred to as the Hexapod, is employed, as shown in Fig. 4. The Hexapod consists of six independently actuated hydraulic cylinders that enable unconstrained motion of the traverse, allowing independent application of forces and bending moments in all six spatial directions.

However, the Hexapod has a limited uniaxial tensile capacity of 1000 kN, which was close to the predicted ultimate resistance of the joints governed by composite fracture. Therefore, specimens subjected to pure uniaxial tensile loading were instead tested using a custom-built 6 m high loading frame with a maximum load capacity of 2.5 MN, as shown in Fig. 5. The machined flanges are connected via pinned hinges to a hydraulic jack at the top and to the steel frame at the bottom.

2.3. Loading scenarios and instrumentation

The joint specimens are tested under three loading cases, resulting in a failure envelope for axial loading and in-plane bending. The complete test matrix, including loading types (F_z denotes axial force and T_y denotes in-plane bending moment) and prescribed loading rates, is summarized in Fig. 6. All tests are conducted under quasi-static conditions using displacement and/or rotation control via hydraulic actuators. Both the applied force and actuator stroke are continuously recorded throughout the tests.

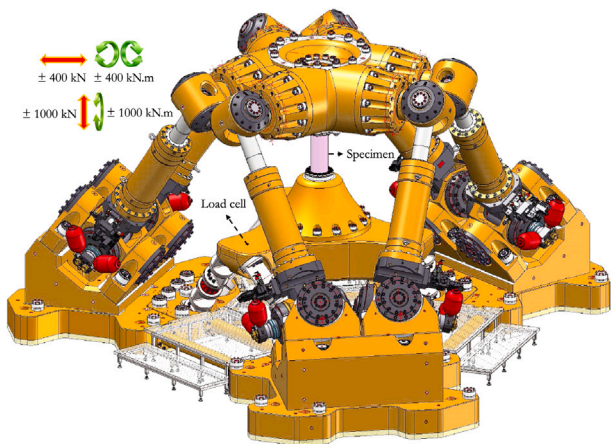


Fig. 4. Six-degree-of-freedom Hexapod testing system enabling the application of uniaxial bending and combined axial force-in-plane bending loading.

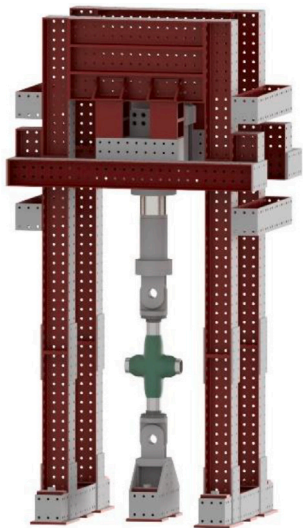
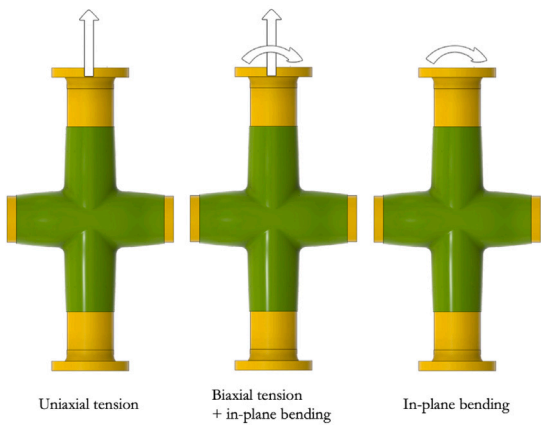


Fig. 5. Custom-built loading frame used for uniaxial tensile testing of X90 joint specimens.

Since actuator stroke does not accurately reflect the specimen's deformation, three-dimensional Digital Image Correlation (3D-DIC) is employed to measure surface deformation and strain fields. A GOM Aramis 12 MP system with an adjustable base, equipped with 12 mm lenses and polarized blue LED illumination, is used for this purpose. The DIC technique enables quantification of absolute and relative displacement fields, identification of strain localization, monitoring of crack initiation and propagation, and measurement of crack-opening parameters, which are particularly relevant for characterizing composite fracture. To ensure full-field monitoring of the joint, two DIC systems are deployed for each specimen, as cracking may initiate in any region of the composite wrap. Accordingly, a speckled pattern is applied over the entire composite surface. Fig. 7 illustrates the X90 joint mounted in the Hexapod test frame, with arrows indicating the directions of applied displacements and rotations at the top of the specimen, where the testing system prescribes the loading. The DIC systems are positioned to capture both sides of the joint (positive and negative Y-directions). This orientation provides optimal visibility of the composite surface, which is susceptible to fracture during both in-plane bending and combined axial-in-plane bending loading.



Loading speed	1.0 mm /min	0.15 mm /min 0.066 °/min	0.132 °/min
Number of specimens	2	2	2
Load	Fz	Fz+Ty	Ty

Fig. 6. Experimental loading matrix for composite fracture joint specimens.

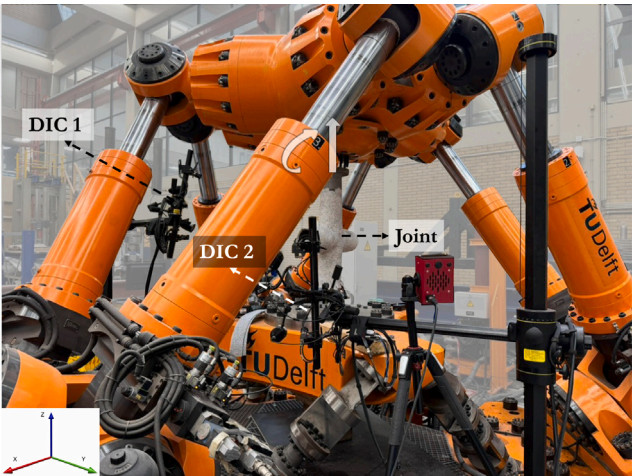


Fig. 7. Hexapod test set-up with the mounted joint specimen and dual 3D-DIC instrumentation.

For the uniaxial tension tests, two 3D-DIC systems are again employed, positioned at the front and rear of the specimen, as shown in Fig. 8. In addition, all joints are instrumented with distributed fibre optic sensing based on Rayleigh backscattering. In this technique, the optical fibre functions as a continuous strain sensor by detecting changes in the properties of the backscattered light along its length, resulting from local variations in strain or temperature [25]. In the present study, the optical fibres are used exclusively to measure longitudinal strain. Their placement within the composite wrap is also illustrated in Fig. 8.

During manufacturing, the optical fibres, with a total diameter of 195 µm including the coating, are embedded between the second and third plies inward from the steel-composite interface. Given their very small size relative to the laminate thickness, their influence on interface behaviour is considered negligible. Previous studies on wrapped composite joints show that debonding typically initiates near the mid-length of the joint and propagates outward along the bonded interface [26]. Positioning the fibre along this expected damage path allows

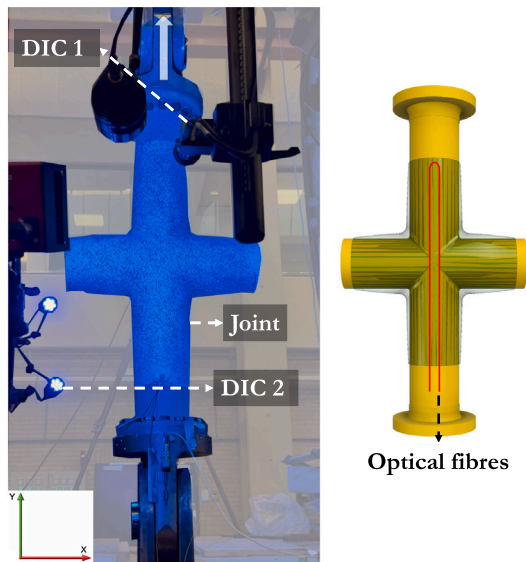


Fig. 8. Instrumentation of the uniaxial tensile test, showing the placement of the dual 3D-DIC systems and embedded optical fibres.

direct observation of the evolving strain field. It enables more accurate quantification of the debonded region compared to conventional surface-based monitoring techniques. In the case of the thin composite wrap specimen tested here, the optical fibres can also detect local strain peaks at the location of the through-thickness fracture. Longitudinal strain measurements are acquired using a LUNA ODiSI 7100 interrogator [27] in combination with ORMOCER-coated glass fibres supplied by FBGS [28]. The system provides a spatial resolution of up to 0.65 mm and employs low-bend-loss 125 μm optical fibres. Beyond the present experimental campaign, embedding fibres during manufacturing demonstrates strong potential for structural health monitoring of in-service jacket structures, enabling continuous condition assessment throughout their service life. This methodology is currently being investigated within the Composmart project in Delft.

During testing, data are acquired simultaneously from three synchronized measurement systems: (i) the testing machine, which records actuator load and displacement, (ii) the 3D-DIC systems, and (iii) the fibre-optic sensing system. The testing machine controls the timing of all measurements via an integrated trigger script, ensuring consistent, synchronized data acquisition across the three systems.

3. Numerical modelling

The experimental joint configuration is numerically modelled using Abaqus/Explicit version 6.21. This section provides a comprehensive description of the numerical framework, including the selected analysis approach, geometric representation, material models, mesh generation strategy, boundary and loading conditions, and adopted contact definitions.

3.1. Geometry, element type and mesh generation

The numerical geometry remains identical to that used in the experimental programme. The brace members, chord member, machined flanges, and composite wrap are modelled as separate components and later assembled to form the complete joint model. As highlighted by [20], the use of solid layered elements for modelling composite wraps presents several limitations for this type of joint. The study also emphasizes that accurate representation of wrapped composite joints requires modelling the composite wrap using solid elements rather than shell formulations. Following this, the composite wrap is represented as

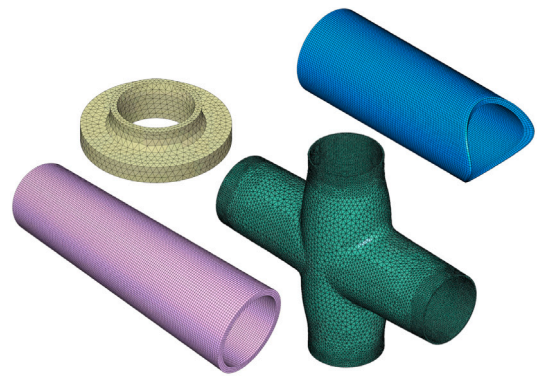


Fig. 9. FE model details showing mesh density variations.

a homogenized three-dimensional solid continuum, rather than through a layered ply-level description or shell-based modelling approach.

For the brace and chord members, three-dimensional eight-node linear solid elements with reduced integration (C3D8R) are adopted. Following the recommendations in [29], C3D8R element formulation is well suited for explicit analyses, offering a balance between numerical accuracy and computational efficiency. A global element size of 4 mm is employed for these steel components. To accurately capture bending deformation, the tubular members are discretized using four elements through the wall thickness.

The composite wrap and the machined flanges are meshed using four-node linear tetrahedral elements (C3D4), selected for their geometric versatility for complex three-dimensional geometries. The composite wrap is assigned a global mesh size of 10 mm, while the inner surface in contact with the steel members is locally refined to 4 mm to accurately capture through-thickness shear deformation. In the tapered regions of the composite wrap, as shown in Fig. 2, the mesh transitions smoothly from 10 mm to 4 mm. The flanges connected to the brace members are discretized with a global mesh size of 12 mm, with local refinement to 8 mm at the contact interface with the braces. The selected element types and mesh densities are based on a mesh sensitivity study reported by [30], which considers an optimal compromise among numerical accuracy, computational cost, and joint dimension. An overview of the resulting finite element mesh for each component is presented in Fig. 9.

3.2. Material model

In the numerical framework, two distinct material models are assigned to represent the steel and composite components of the joint. The brace members, chord member, and flanges are modelled using material properties corresponding to S355 structural steel. An isotropic elasto-plastic constitutive model is adopted, with Young's modulus $E = 210$ GPa and Poisson's ratio $\nu = 0.3$. Plastic behaviour is defined using a nominal stress-strain relationship, assuming yielding initiates at 355 MPa and is followed by linear isotropic hardening up to an ultimate stress of 510 MPa at 15% plastic strain.

The composite wrap is modelled as a transversely isotropic material governed by an elasto-plastic constitutive law with isotropic hardening. The properties listed in Table 1 are based on material characterization tests reported by [20]. The through-thickness elastic properties, E_3 and $\nu_{13} = \nu_{23}$, are derived using Classical Laminate Theory for an equivalent unidirectional laminate. The fibre volume fraction is taken as $V_f = 30\%$, based on burn-off tests.

The strength of the composite material in different in-plane and through-thickness directions is represented by assigning anisotropic plasticity to solid elements. In-plane tensile, compressive, and through-thickness shear strength are considered to be most influential for the

Table 1
Elastic material properties of the composite laminate adopted in the numerical model.

Material	Mechanical properties	Average value (and CoV [%])
Composite laminate	In-plane tensile strength in x/y direction – $f_{x,t} = f_{y,t}$	216 MPa (5.78)
	In-plane tensile modulus in x/y direction – $E_{x,t} = E_{y,t}$	11798 MPa (6.37)
	In-plane compressive strength in x/y direction – $f_{x,c} = f_{y,c}$	200 MPa (3.79)
	In-plane compressive modulus in x/y direction – $E_{x,c} = E_{y,c}$	12077 MPa (4.50)
	In-plane shear strength – $f_{xy,v}$	72.2 MPa (2.59)
	In-plane shear modulus – G_{xy}	3120 MPa (6.81)
	Out-of-plane shear strength – $f_{xz,v}$	35.74 MPa (4.85)
	Out-of-plane shear modulus – $G_{xz} = G_{yz}$	2686 MPa (7.35)
	Poisson's ratio – ν_{xy}	0.224
	Poisson's ratio – $\nu_{xz} = \nu_{yz}$	0.351

joint behaviour. Values of those properties are given in Table 1. The anisotropic nature of yielding in the composite is represented using the Hill yield potential, which extends the classical von Mises criterion to account for different strengths of the laminate in the principal material directions. The Hill yield function shown in Eq. (1) introduces the concept of yield stress ratios, leading to direction-dependent yielding behaviour in the three-dimensional principal stress space. The adopted yield stress ratios are derived from the composite material test data presented in [20].

$$f(\sigma) = \sqrt{F(\sigma_{22} - \sigma_{33})^2 + G(\sigma_{33} - \sigma_{11})^2 + H(\sigma_{11} - \sigma_{22})^2 + 2L\sigma_{23}^2 + 2M\sigma_{31}^2 + 2N\sigma_{12}^2} \quad (1)$$

The coefficients F , G , H , L , M , and N are functions of the yield stress ratios R_{11} , R_{22} , R_{33} , R_{12} , R_{13} and R_{23} following the formulation implemented in Abaqus, and are defined as follows:

$$F = \frac{1}{2} \left(\frac{1}{R_{22}^2} + \frac{1}{R_{33}^2} - \frac{1}{R_{11}^2} \right), \quad G = \frac{1}{2} \left(\frac{1}{R_{33}^2} + \frac{1}{R_{11}^2} - \frac{1}{R_{22}^2} \right), \quad H = \frac{1}{2} \left(\frac{1}{R_{11}^2} + \frac{1}{R_{22}^2} - \frac{1}{R_{33}^2} \right) \quad (2)$$

$$L = \frac{3}{2R_{23}^2}, \quad M = \frac{3}{2R_{13}^2}, \quad N = \frac{3}{2R_{12}^2} \quad (3)$$

The yield stress ratios R_{ij} are defined in terms of the directional yield stresses $\bar{\sigma}_{ij}$ as

$$R_{11} = \frac{\bar{\sigma}_{11}}{\sigma_0}, \quad R_{22} = \frac{\bar{\sigma}_{22}}{\sigma_0}, \quad R_{33} = \frac{\bar{\sigma}_{33}}{\sigma_0}, \quad R_{12} = \sqrt{3} \frac{\bar{\sigma}_{12}}{\sigma_0}, \quad R_{13} = \sqrt{3} \frac{\bar{\sigma}_{13}}{\sigma_0}, \quad R_{23} = \sqrt{3} \frac{\bar{\sigma}_{23}}{\sigma_0} \quad (4)$$

where $\bar{\sigma}_{ij}$ denotes the yield stress when $\bar{\sigma}_{ij}$ is the only non-zero stress in the six stress components, and σ_0 represents the user-defined yield stress in Abaqus.

The anisotropic yield surface defined by the Hill plasticity potential, illustrated schematically in Fig. 10, is adopted as the plastic yielding criterion for the composite laminate in the present study. The shape of the yield surface is determined by stress ratios. Stress states located within this surface correspond to elastic behaviour, whereas yielding initiates when the stress state reaches the yield surface. Beyond yield onset, the composite material's further degradation is governed by a ductile damage formulation with distinct criteria for damage initiation and damage evolution.

Damage initiation in the composite laminate is defined using an equivalent plastic strain criterion, with the onset of damage occurring when the equivalent plastic strain reaches $\bar{\epsilon}_0^{pl} = 0.02$, dependent on the local stress triaxiality θ . This threshold is calibrated to coincide with the final non-linear point of the calibrated stress–strain response under the Hill plasticity model, beyond which only a small additional increment of equivalent plastic strain is required to trigger element deletion, representing the onset of physical cracking. The equivalent plastic strain at fracture, $\bar{\epsilon}_f^{pl} = 0.17$, is adopted from previous studies calibrated for debonding-dominated failure for $\theta = 1/3$ (uniaxial tension). Damage evolution is governed by a mode-independent, displacement-based law with linear softening, characterized by an equivalent plastic

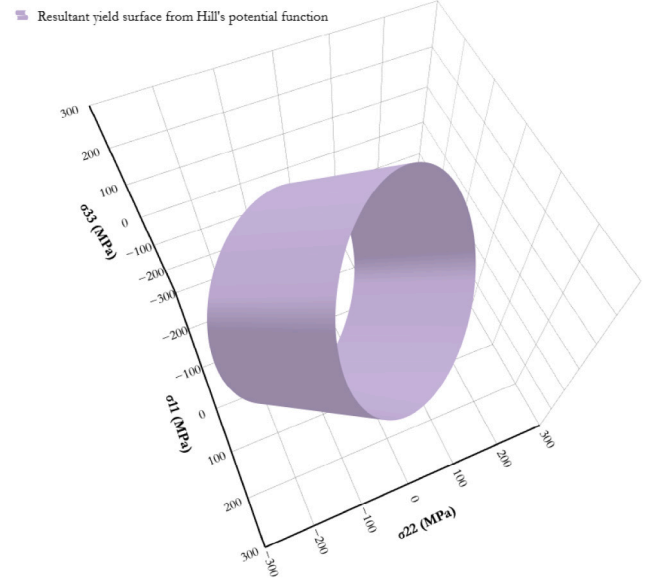


Fig. 10. Schematic representation of the Hill yield surface in principal stress space.

displacement at failure, $\bar{u}_f^{pl} = 0.05$, following the methodology proposed by [31].

In this framework, damage initiation marks the onset of micro-cracking within the composite material. Following initiation, damage evolves, leading to a progressive reduction in material stiffness with increasing deformation. When the critical plastic displacement is reached, the material is assumed to be fully degraded and can no longer sustain further load. A smeared-crack approach is adopted [32], in which cracking is not represented as a discrete geometric discontinuity. Instead, crack initiation, propagation, and directionality emerge implicitly from the constitutive response governed by stress or strain-based failure criteria. The composite material is therefore treated as a continuum throughout the analysis, with its mechanical properties, namely stiffness and strength, progressively degraded as damage accumulates to account for the effect of cracking. This degradation is implemented via field variables that continuously update the elastic constants as the damage state evolves. At the onset of loading, the laminate exhibits its full elastic stiffness. As local strains exceed the damage threshold, the field variables trigger a gradual reduction in elastic moduli and shear stiffness, reflecting the initiation, growth, and coalescence of micro-cracks. This modelling strategy enables a continuous transition from undamaged elastic behaviour, through anisotropic plasticity, to fully degraded material response, without the need to explicitly prescribe crack paths within the finite element mesh.

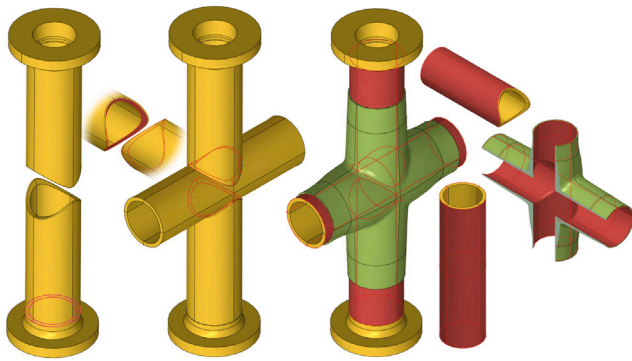


Fig. 11. FE model showing contact pairs and tied interface regions.

3.3. Interactions, boundary conditions and loading

The joint assembly comprises multiple materials, complicating their interactions. Interactions between all steel components in contact are modelled using the general contact formulation in Abaqus/Explicit. A hard pressure overclosure relationship is adopted for the normal contact behaviour, while tangential behaviour is represented using a penalty friction formulation with a friction coefficient of 0.3.

Interactions between the composite wrap and steel surfaces are defined using the same normal contact behaviour, combined with a penalty-based tangential formulation with a friction coefficient of 0.55, derived from an experimental tribological study reported by [17]. In addition, cohesive behaviour is introduced at the steel–composite interface to capture debonding phenomena. A four-linear traction–separation law is introduced to represent the progressive fracture process of the bonded interface. It incorporates mode mixity, defined through power-law interpolation between Mode-I and Mode-II dominated interface behaviour, with maximum tractions and total fracture toughness values determined from dedicated fracture mechanics characterization tests, namely Double Cantilever Beam (DCB) and End-Notched Flexure (ENF) tests, reported in [20]. The interfaces between the machined flanges and the brace members are modelled using tie constraints. An overview of the contact definitions and interaction assignments adopted in the numerical model is shown in Fig. 11.

The three loading scenarios are simulated in Abaqus by applying displacement and/or rotation-controlled, symmetric loading through two reference points located at the ends of the flanges and kinematically coupled to the corresponding flange surfaces. Although loading is applied at one end of the joint in the experimental campaign, a symmetric loading scheme is adopted in the numerical model. For a given mass-scaling and analysis time, the symmetric scheme reduces the net inertial imbalance in the explicit solution and therefore approaches quasi-static conditions more closely, while also improving computational efficiency. A direct comparison of the two schemes for the in-plane bending case, as an example, is presented in Section 4.3.

The analysis is performed by applying a tensile displacement and/or rotation to the top and bottom reference points defined on the flange planes, as shown in Fig. 12. Abaqus/Explicit solver is utilized to resolve the highly non-linear material behaviour, large deformations, and complex contact and cohesive interactions present in the model. To suppress inertial effects, mass scaling is adopted in accordance with the robust nonlinear analysis framework developed by the TU Delft research group. Smooth, time-dependent amplitude functions are used, with an artificial analysis duration of 300 s to ensure quasi-static loading conditions.

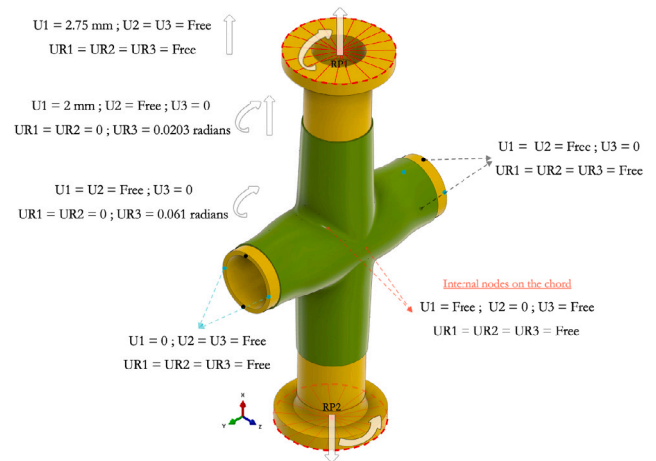


Fig. 12. FE model depicting boundary and loading conditions.

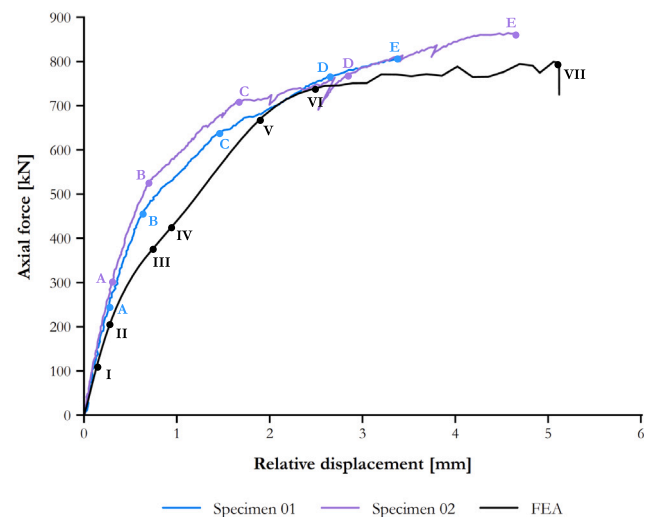


Fig. 13. Force–displacement response under uniaxial tensile loading.

4. Results and discussion

4.1. Uniaxial tensile loading

The force–displacement responses obtained from the two uniaxial tensile experiments and the corresponding finite element simulation are presented in Fig. 13.

At first glance, both experimental specimens exhibit a similar global force–displacement response, indicating good test repeatability. Specimen 02 reaches a higher ultimate resistance of 864.55 kN, whereas Specimen 01 fails at 808.58 kN, corresponding to a 6.47% lower ultimate load and a noticeably reduced ductility. Despite this difference in peak response, both specimens show comparable transitions to different slopes, marked by points A, B, C, and D in Fig. 13. Point E corresponds to the stage immediately preceding final failure, after which a sharp drop in load is observed. These characteristic points in the experimental curves are further analysed using optical fibre strain measurements and full-field surface strain data from DIC to interpret the underlying damage mechanisms and physical behaviour.

The numerical simulation predicts an ultimate resistance of 801 kN, which is 7.35% less than the experimentally measured peak load for Specimen 02, demonstrating the potential of the adopted modelling strategy under relatively simple uniaxial tensile loading. However,

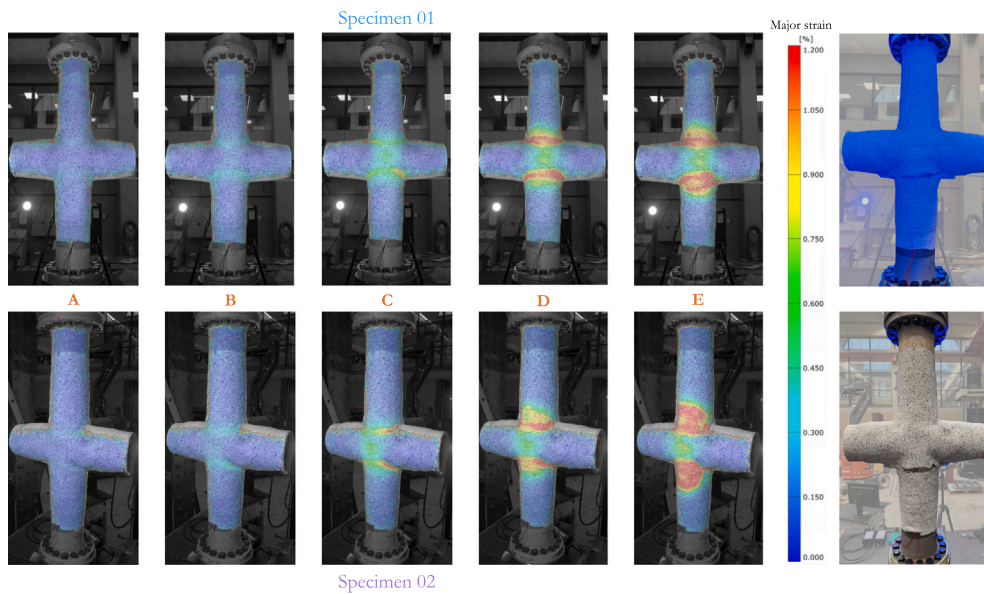


Fig. 14. Experimental major principal strain fields from 3D-DIC at selected stages of uniaxial tensile loading, with the corresponding failure modes.

discrepancies are observed in the graph's slope where divergence between the numerical and experimental responses becomes apparent at relatively low load levels, around 110 kN. The transition identified as point III in the numerical response, which appears qualitatively comparable to point B in the experiments, occurs at a lower force level and at a higher displacement. Furthermore, beyond point VI corresponding to point C in the experimental curves, the numerical model exhibits a relatively stable plateau response. In contrast, the experimental response continues to exhibit a monotonic increase in load until composite fracture occurs. These differences indicate that, while the global strength is fairly captured, the numerical model does not fully reproduce the stiffness evolution and damage progression observed experimentally under uniaxial tension. To quantify the agreement between the numerical and experimental load–displacement responses, Root Mean Square Error (RMSE) and Normalized Root Mean Square Error (NRMSE) values are computed over the common displacement range, yielding values of 61.5 kN and 86.3 kN (NRMSE: 7.6% and 10.1%) for specimens 01 and 02, respectively.

The major principal strain fields obtained from DIC corresponding to the marked stages on the force vs displacement graph are depicted in Fig. 14. At point A, both specimens exhibit a low and nearly uniform strain distribution across the composite surface. The force–displacement response remains linear, indicating predominantly elastic behaviour at the steel–composite interface. The absence of localized strain concentrations suggests that load transfer is initially well distributed and governed by elastic interfacial shear, with no evidence of interfacial debonding or composite cracking.

Beyond point A, localized strain concentrations begin to develop near the brace–chord intersections of the X-joint. Following classical adhesively bonded joint mechanics, the sharp re-entrant corners of the joint suffer higher interlaminar shear and peel stresses. The change in stiffness observed between points A and B could therefore be attributed to the combined effects of degradation of the temporary structural adhesive between brace and chord members and the initiation of debonding at the steel–composite interface.

At point C, the formation of a pronounced high-strain band indicates progressive interfacial degradation and a reduction in the effective bonded area, resulting in increased load transfer through the remaining bonded regions. As loading continues, this high-strain region expands

asymmetrically, suggesting both continued propagation of the debonding front and the onset of nonlinear deformation within the composite material. The appearance of load drops and kinks in the force–displacement response, particularly evident for Specimen 02 together with audible fibre cracking during testing, confirms the activation of damage mechanisms within the composite beyond this point.

Approaching point E, narrow and highly localized strain bands develop across the brace–chord intersection, consistent with the formation of a macroscopic composite fracture that governs final failure as shown in Fig. 14. The similarity of the strain patterns observed in both specimens indicates a repeatable damage mechanism dominated by joint geometry rather than material variability. Moreover, Fig. 13 and the gradual evolution of strain localization demonstrate that composite fracture in these joints occurs in a ductile manner, providing a clear warning before the impending failure.

To enable a direct comparison between the major principal strain fields obtained from experimental DIC and those predicted by the finite element model, a custom Abaqus Python post-processing script is developed. The script exports the evolving deformed surface geometry from the Abaqus output database in a GOM-compatible format. The undeformed configuration is exported for the initial frame, while subsequent frames are generated by updating the nodal coordinates through superposition of the displacement field onto the reference geometry. The resulting strain fields from the numerical model are presented in Fig. 15 and correspond to the critical stages identified in the force–displacement response. In addition to the surface strain contours, the figure also depicts the evolution of the damage variable (CSDMG) from the finite element analysis, providing insight into the initiation and progression of damage at the steel–composite interface.

The numerically predicted major principal strain patterns follow trends similar to those observed experimentally up to failure. A closer examination of interfacial degradation reveals that the first deviation from the experimental response, occurring at approximately 110 kN (Point I), is associated with failure of the temporary adhesive between the brace and chord members, as shown in Fig. 15. With increasing load, degradation of this adhesive progresses, leading to the initiation of debonding at the chord–composite interface (Point II).

Once debonding initiates, a pronounced reduction in stiffness is observed in the force–displacement response until point III, where debonding intensifies and initiates at the brace–composite interfaces.

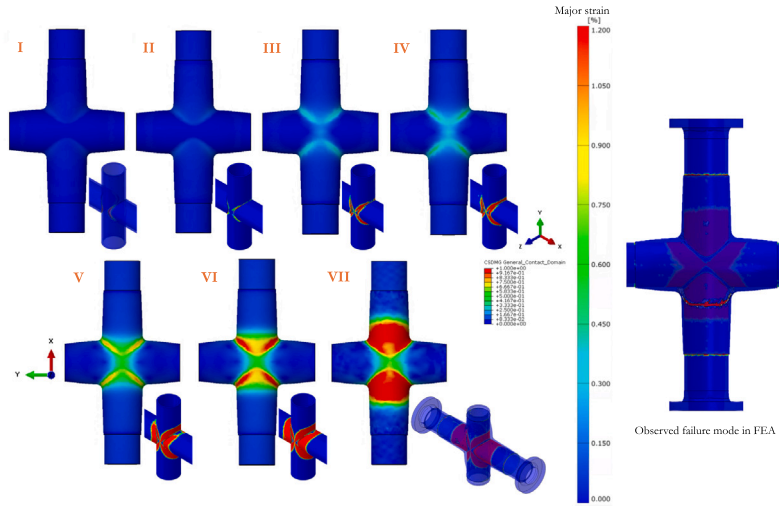


Fig. 15. Evolution of major principal strain fields and interfacial damage predicted by the FE model under uniaxial tensile loading.

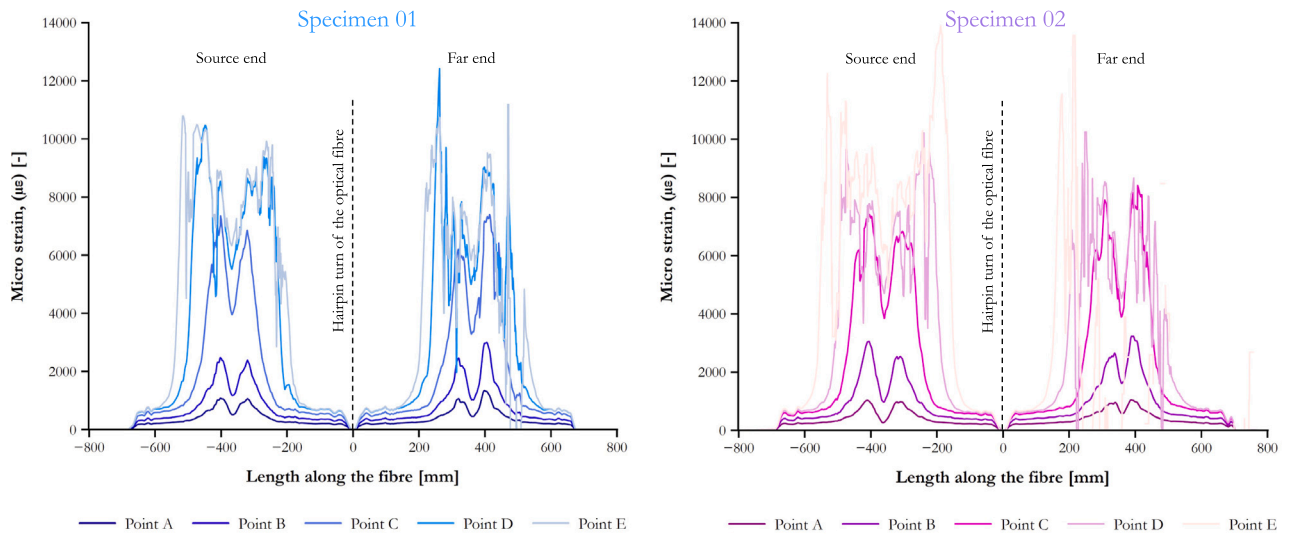


Fig. 16. Experimentally measured longitudinal strain distributions along the optical fibre at selected loading stages during uniaxial tensile testing.

This sequence supports the experimental observation that the stiffness change between points A and B is governed by similar physical mechanisms. Between points III and IV, the numerical model highlights coalescence of debonding on both sides of the chord, a behaviour that is less identifiable in the experimental response.

Beyond point IV, continued debonding, particularly along the braces, leads to a gradual increase in joint stiffness, corresponding to the experimental response between points B and C. Further coalescence of debonding along the braces occurs between points V and VI, analogous to the earlier chord debonding coalescence. Beyond point VI, progressive damage occurs within the composite material, accompanied by a plateau in the force–displacement response. At point VII, composite fracture becomes evident through element deletion in the numerical model, after which abrupt failure occurs. The predicted final failure mode is consistent with experimental observations as shown in Fig. 15.

The experimentally measured longitudinal strain distributions for both specimens, together with the corresponding finite element predictions at the optical fibre locations, with focus on the marked critical stages, are shown in Figs. 16 and 17. These results primarily capture changes in longitudinal strain distribution and the progression of crack growth along the fibre path. For the experimental measurements, the

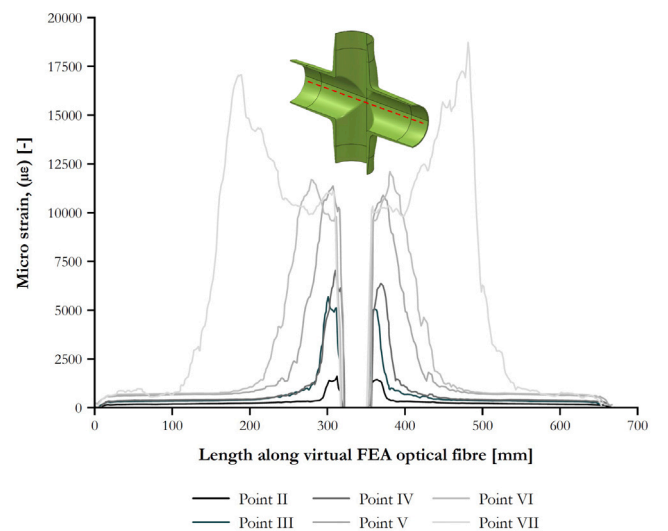


Fig. 17. FE prediction of longitudinal strain distributions under uniaxial tensile loading.

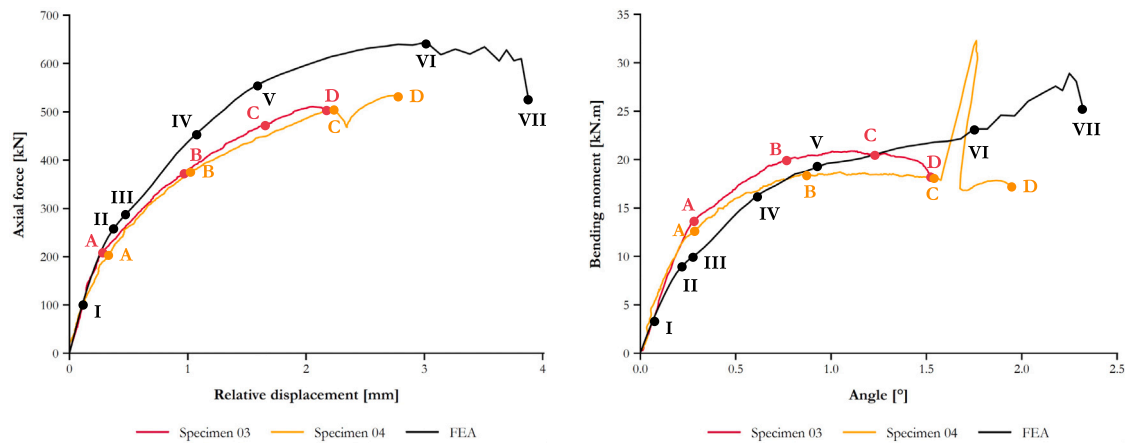


Fig. 18. Load-deformation response under biaxial tension + in-plane bending loading.

source end refers to the beginning of the optical fibre where the interrogation signal is introduced, while the far end corresponds to the terminal point from which a return signal is detected before the light exits the system or becomes fully attenuated. In the numerical simulations, the hairpin turn of the fibres are neglected, and strain results are reported for a single fibre end. Owing to the symmetric nature of debonding growth, these results may be directly compared with either the source or far-end measurements from the experiments. The longitudinal strain component (LE11) in the finite element model is extracted at a depth of 1.5 mm within the composite, measured from the surface in contact with the steel components. Comparison of the experimental and numerical results indicates that the finite element model predicts higher longitudinal strain levels in the composite prior to fracture. Despite this difference, both experiments and numerical simulation exhibit similar debonding patterns along the fibre length. In particular, points B to E in the experiments and points IV to VII in the numerical simulations show closely corresponding strain localization patterns associated with progressive debonding. Furthermore, Specimen 02 exhibits higher longitudinal strain levels than Specimen 01, in agreement with the surface strain observations reported in Fig. 14.

It is further observed from both figures that, at a longitudinal strain of approximately $1800 \mu\epsilon$, the predicted debonding lengths along the optical fibre show close agreement between experiments (421 mm) and simulations (408 mm). In addition, the absence of strain development at the mid-length of the composite wrap in the finite element results is attributed to a geometric discontinuity in the model, where the composite material is not present in this region along the virtual optical fibre.

4.2. Biaxial tension + in-plane bending loading

The load-deformation response of the X-joints subjected to combined axial tension and in-plane bending is shown in Fig. 18, together with the corresponding numerical predictions. Similar to the uniaxial tensile case, several critical points are identified in the response, marked by changes in slope of the load-deformation curve. While the axial response of the experimental specimens is highly repeatable, a mild scatter is observed in the bending moment capacity. This scatter is attributed to the distinct mechanisms governing in-plane bending resistance in the composite wrap. Under combined axial-bending loading, the crown region on the tensile side of bending experiences the highest stress and is dominated by through-thickness stress components, promoting mixed-mode (Mode I/Mode II) interlaminar fracture at the sharp laminate corners. In contrast, the crown region on the compressive side of bending, which carries a significant portion of the axial tensile force, is primarily governed by in-plane stress components.

As a result, the bending response is more sensitive to local manufacturing-induced heterogeneities, such as variations in resin distribution and fibre waviness, which directly influence through-thickness stiffness and strength. These effects introduce specimen-to-specimen variability in the moment-rotation response. Nevertheless, the scatter in peak bending moment remains within approximately 20%, which is relatively modest compared with the variability typically reported for interlaminar fracture properties in composite laminates.

The joint response remains linear elastic up to point A, beyond which a deviation from linearity indicates the onset of localized interfacial damage in the crown region. At point B, the first evidence of debonding at the saddle location is detected from the optical fibre measurements, indicated by an increase in the fibre length associated with elevated microstrain levels (Fig. 20). Between points B and C, a pronounced increase in debonding occurs, as evidenced by an expansion of high-strain regions along the fibre length. The optical fibre data further reveal asymmetric debonding around the brace circumference, with the source end exhibiting a longer strained region than the far end, highlighting the sensitivity of fibre sensing to non-uniform damage development. Between points C and D, the rate of debonding propagation decreases, while strain levels continue to rise at existing damaged locations, indicating increasing stress concentrations within the composite material. Surface strain fields at point D show that load transfer is mobilized over an increasingly larger circumferential extent of the composite, suggesting a redistribution of stresses around the joint. This behaviour is consistent with observations reported by [33] for welded tubular joints subjected to combined axial and in-plane bending. The transient spike observed in the bending moment response of Specimen 04 is considered an anomaly attributed to internal stress redistribution upon debonding progression and does not represent the physical ultimate resistance of the joint. The failure envelope points in Fig. 30 are computed as the average of the maximum resistances of the two specimens tested for each loading case. For the combined loading case, the average of the maximum axial forces and the average of the maximum bending moments define the envelope point, with the maximum bending moment of Specimen 04 identified with the transient spike excluded. The final failure mode, shown in Fig. 19, is characterized by crack initiation at the crown region followed by propagation towards the saddle.

Comparison of the strain fields for the two experimental specimens shows that Specimen 04 develops a larger extent of elevated strains, which also indicates asymmetric debonding, as shown in Fig. 19. This observation supports the optical fibre measurements (Fig. 20), which similarly indicate non-uniform debonding around the brace circumference. Between points C and D, the surface strain fields reveal the formation of a dominant debonding front propagating along the

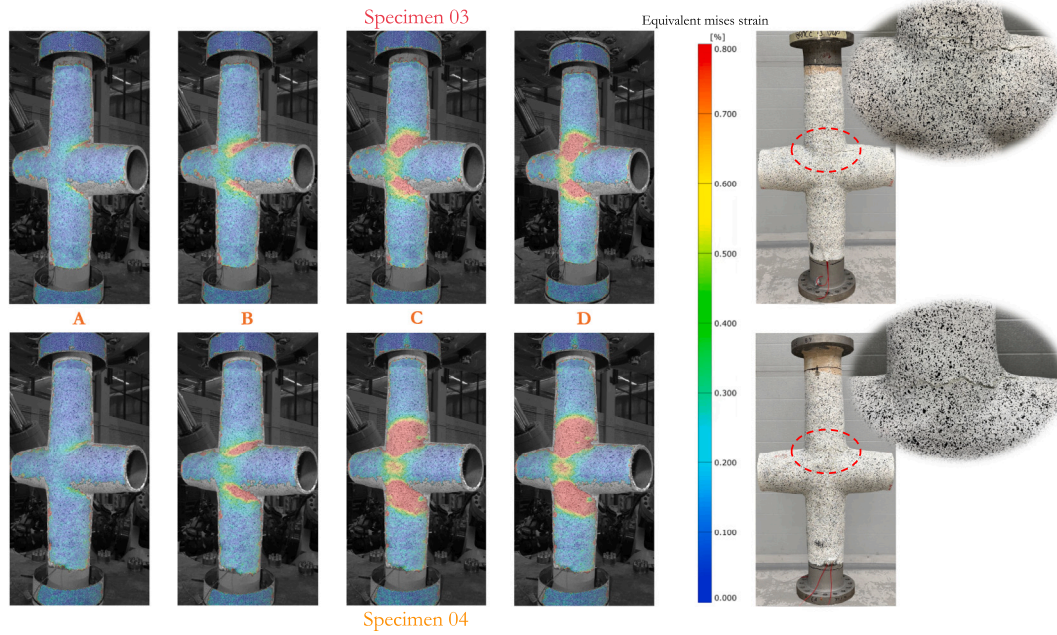


Fig. 19. Experimental equivalent mises strain fields from 3D-DIC at selected stages of combined loading, with the corresponding failure modes.

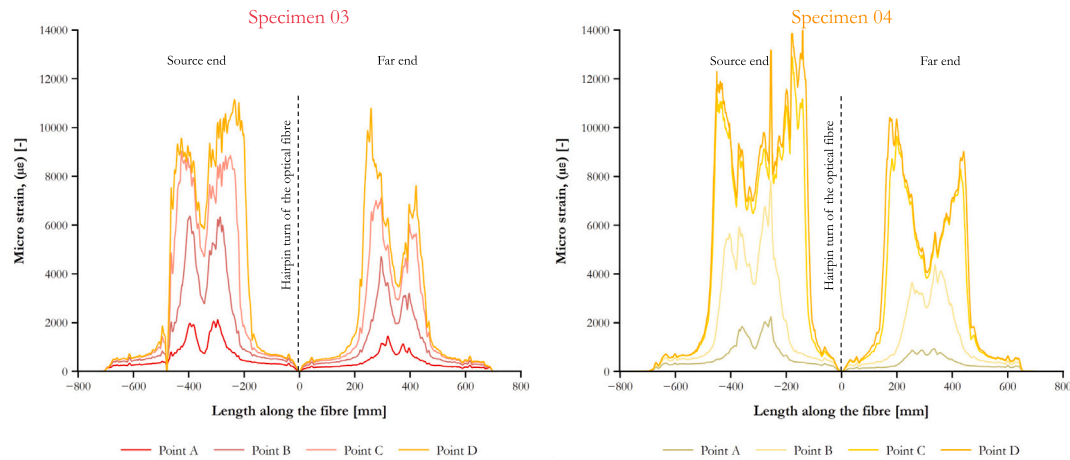


Fig. 20. Experimentally measured longitudinal strain distributions along the optical fibre at selected loading stages during combined axial tension and in-plane bending.

brace members. Furthermore, the moment–rotation response exhibits a plateau or slight softening over this interval, indicating stable crack growth within the composite and along the steel–composite interface, accompanied by a progressive reduction in the effective bonded area.

The numerical simulations predict a maximum axial force of 645 kN and a maximum bending moment of 28.96 kN m, which are respectively 20.79% and 36.94% higher than the experimentally measured values. The numerical-experimental discrepancy is further quantified using RMSE and NRMSE, computed over their common deformation range. For axial force response, RMSE values of 65.7 kN and 83.5 kN (NRMSE: 12.9% and 15.7%) are obtained for specimens 03 and 04, respectively. The corresponding bending moment RMSE values are 1.9 kN m and 2.8 kN m (NRMSE: 9% and 15.1%). Similar to the uniaxial loading case, the strain fields from the numerical model are shown in Fig. 21, together with the corresponding damage variable (CS-DMG) at the critical stages identified in the load–deformation response.

At point I (approximately 100 kN), failure of the temporary adhesive initiates near the crown region. At point II, debonding initiates at the chord–composite interface on the tensile side of the crown, followed by coalescence of debonded regions at point III. From points III to VI, asymmetric debonding progresses along the brace circumference, while debonding continues to develop along the chord on the tensile side of bending. Beyond point VI, minor load fluctuations in the numerical response are associated with crack initiation and propagation within the composite material.

The final failure mode predicted by the numerical simulation is shown in Fig. 21. While the simulations capture the overall damage evolution, the predicted failure modes do not fully coincide with those observed experimentally. In the finite element results, crack initiation predominantly occurs in the brace regions away from the tensile crown. In contrast, the experimental specimens exhibit crack initiation at the tensile crown, followed by propagation through the saddle towards the compression-side crown.

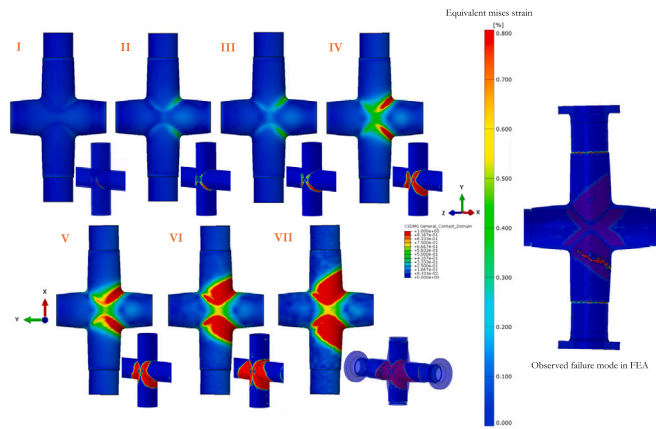


Fig. 21. Evolution of equivalent mises strain fields and interfacial damage predicted by the FE model under combined loading.

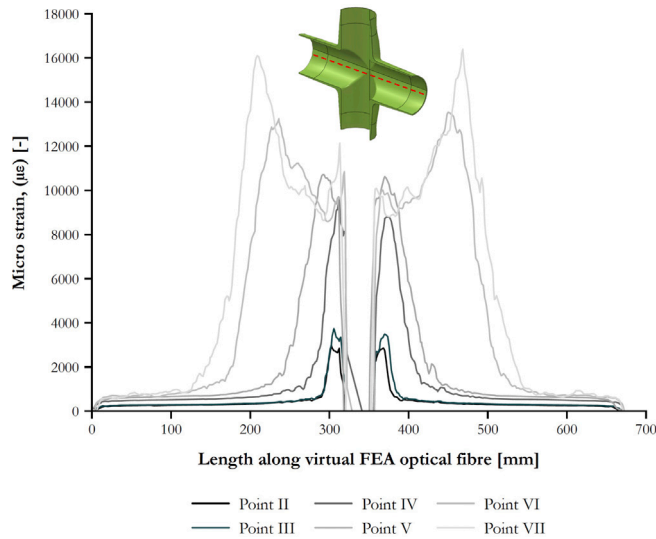


Fig. 22. FE prediction of longitudinal strain distributions under combined axial tension and in-plane bending.

Furthermore, numerical strain results evaluated at the optical fibre locations are presented, with focus on the marked critical stages. The longitudinal strain distributions measured in the experiments for both specimens, together with the corresponding finite element predictions, are shown in Figs. 20 and 22. In the numerical simulations, the hair-pin turns of the fibres are neglected, and strain results are reported for the source end of the fibre, enabling direct comparison with the corresponding experimental measurements.

The comparison of results indicates that the finite element model predicts higher longitudinal strain levels in the composite. Despite this difference in strain magnitude, similar debonding patterns are observed in both experiments and simulations. In particular, points B–C in the experiments and points V–VI in the numerical analysis, corresponding to comparable damage mechanisms, exhibit the largest debonding lengths along the fibre. For both Specimen 03 and Specimen 04, higher longitudinal strains are consistently measured at the source end of the fibre compared to the far end. This asymmetry is attributed to non-uniform circumferential debonding along the brace members, with the source end located closer to the tensile crown region and the far end closer to the compressive crown side.

4.3. In-plane bending loading

The moment–rotation responses obtained from the uniaxial in-plane bending experiments, together with the corresponding numerical predictions, are shown in Fig. 24. The experimental results exhibit a high degree of repeatability in terms of ultimate bending resistance and the occurrence of the identified critical stages. This repeatability indicates consistent specimen quality and suggests that local manufacturing-induced heterogeneities have a limited influence on the in-plane bending response.

Comparison of the major surface strain fields for the two experimental specimens in Fig. 23 shows pronounced asymmetric strain development around the brace circumference, which is more severe than that observed under combined axial tension and in-plane bending. The failure modes under uniaxial in-plane bending are consistent with those reported in Section 4.2, with cracks initiating at the tensile crown region and propagating stably towards the saddle. The experiments reveal a stable composite fracture process, with visible surface cracking detected by DIC at bending moments of approximately 46 kN m for Specimen 05 and 43 kN m for Specimen 06, indicating good repeatability of the tests.

To verify that the symmetric loading scheme does not affect the predicted response, the in-plane bending case includes an additional simulation using single-end loading, in which the rotation is applied at one end of the joint and the opposite end is restrained, as in the experiment. All the other modelling parameters remain unchanged. The moment–rotation responses obtained with the two schemes in Fig. 24 are coincident up to and throughout the nonlinear pre-peak regime. The initial stiffness is indistinguishable, and the peak moments differ by approximately 5%. The predicted failure mode is identical as shown in Fig. 25, with cracking initiating at the chord-brace intersection on the tensile side of the crown and propagating towards the saddle in both models. The adequacy of the quasi-static condition is assessed in Fig. 25, where the bending moments resolved at the ends are plotted against one another. The symmetric model follows a near 1:1 line throughout loading, whereas the single-end model exhibits inertial imbalance between the ends, as visualized by a markedly noisier plot.

The numerical simulations predict an ultimate bending moment of 75.92 kN m, which is approximately 40%–41% higher than the experimentally measured values, with crack initiation in the composite material occurring later in the numerical model, at approximately 62 kN m. The trend of increased strength and ductility observed in the moment–rotation response is similar to that observed for the combined axial tension and in-plane bending case. The numerical-experimental discrepancy quantified using RMSE and NRMSE computed over the common rotation range, yields 2.7 kN m and 5.1 kN m (NRMSE: 4.9% and 9.5%) for specimens 05 and 06, respectively. The low NRMSE values reflect close agreement however, they should be interpreted alongside the peak-load deviation, as the numerical model predicts a substantially longer response than observed experimentally, with the capacity overprediction concentrated beyond the experimental failure point. The numerically predicted major surface strain fields, together with the evolution of the damage variable CSDMG, are shown in Fig. 26.

Although circumferential debonding on the brace members remains asymmetric, the governing debonding mechanisms at failure differ between uniaxial in-plane bending and combined axial tension + in-plane bending. In addition, the influence of temporary adhesive degradation observed in other loading scenarios is negligible for uniaxial in-plane bending. This behaviour arises from the direct contact established between the brace and chord members on the compressive crown side, which dominates the joint stiffness together with progressive degradation of the composite wrap. Unlike the combined axial and in-plane bending case, the failure modes under uniaxial in-plane bending are accurately captured by both the experimental results and the numerical

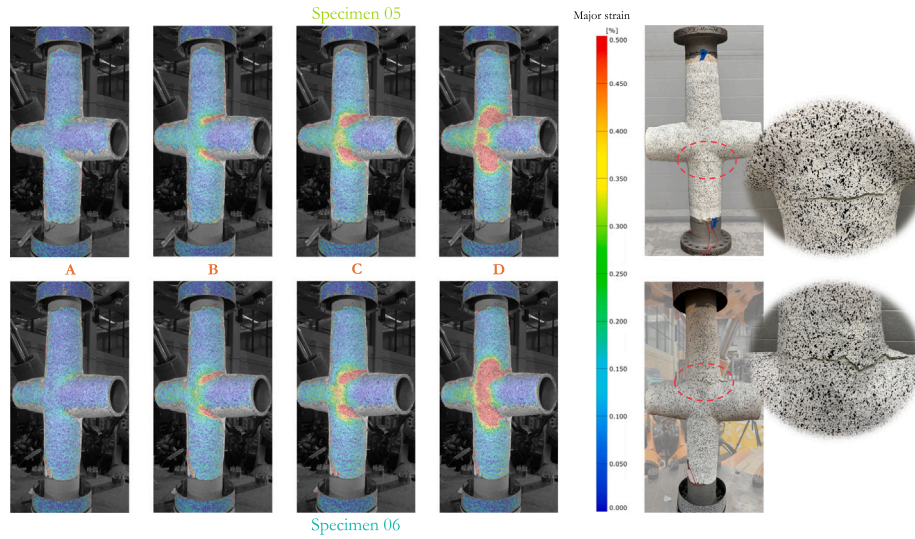


Fig. 23. Experimental major principal strain fields from 3D-DIC at selected stages of in-plane bending loading, with the corresponding failure modes.

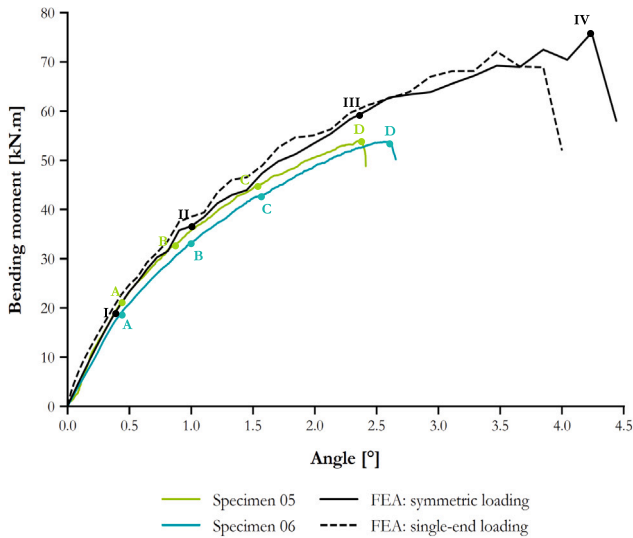


Fig. 24. Moment-rotation response under in-plane bending loading.

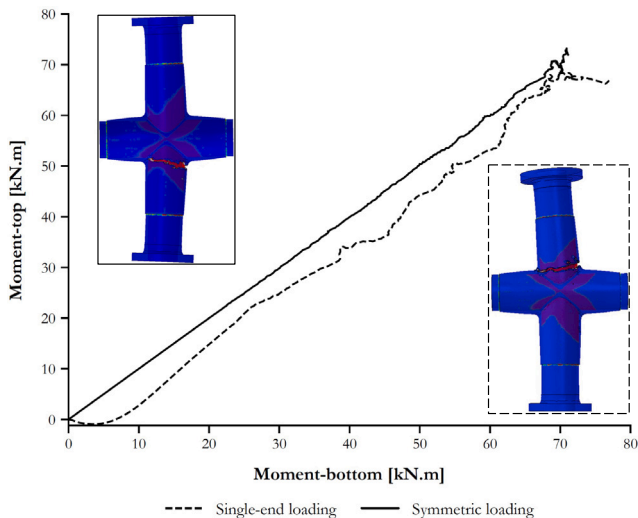


Fig. 25. Comparison of inertial imbalance in single-end and symmetric loading schemes for in-plane bending case and their corresponding failure modes.

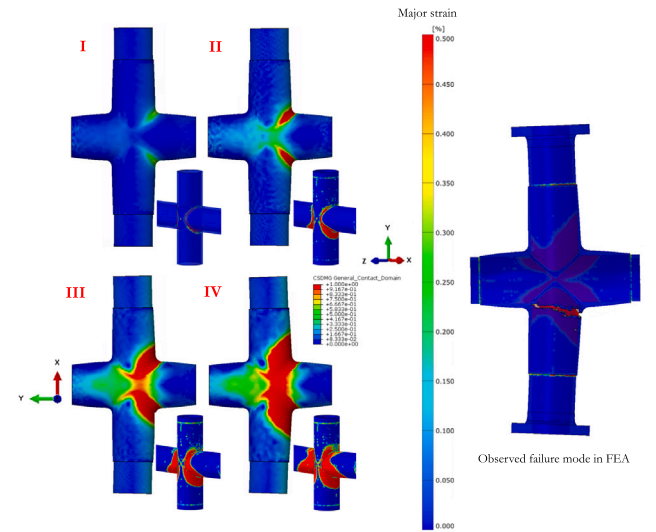


Fig. 26. Evolution of major principal strain fields and interfacial damage predicted by the FE model under in-plane bending.

simulations, indicating improved agreement between observed and predicted damage evolution for this loading configuration.

The optical fibre measurements from the experiments indicate higher longitudinal strains at the source end of the fibre, which is located closer to the tensile crown, while lower strains are observed at the far end, closer to the compressive crown. Comparison with Fig. 20 confirms the development of pronounced asymmetric debonding under uniaxial in-plane bending. At the initial loading stages, negative strain values are recorded at the far end of the fibre, due to direct contact between the brace and chord members, inducing local compressive deformation.

The failure location differs between the two experimental specimens: Specimen 05 exhibits macroscopic cracking near the bottom side of the joint, whereas Specimen 06 fails on the top. This difference results in mirrored strain distributions between the two tests. For clarity, the top and bottom orientations of the joint corresponding to the strain measurements are indicated in Fig. 27. The longitudinal strain results from the numerical simulations, shown in Fig. 28, are extracted from a location comparable to the experimental source end. The numerical model predicts slightly higher peak strain levels than those measured

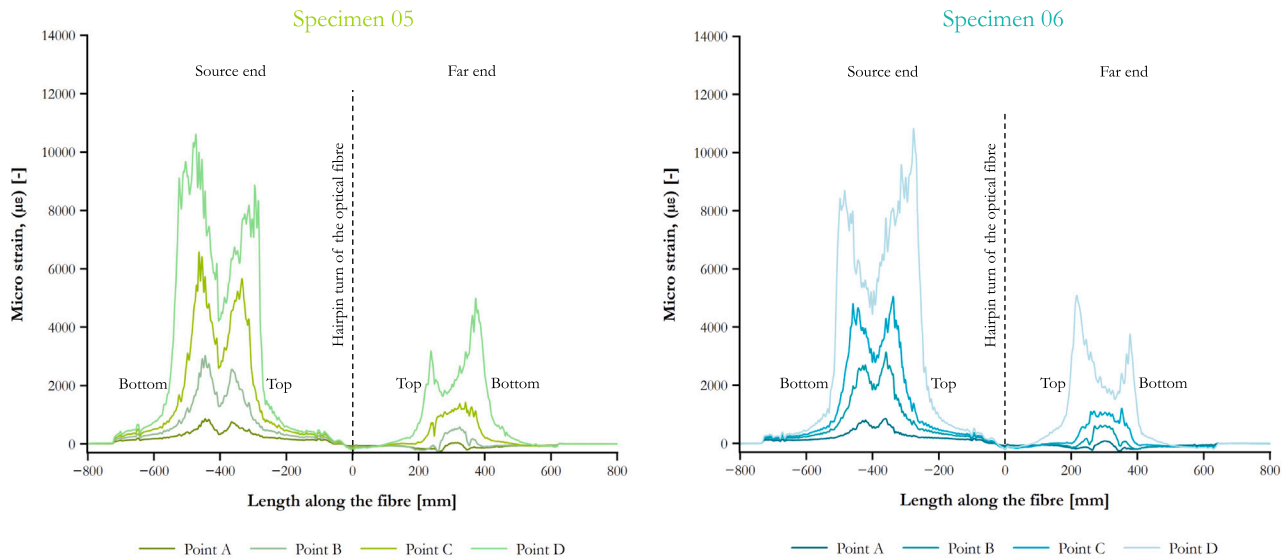


Fig. 27. Experimentally measured longitudinal strain distributions along the optical fibre at selected loading stages during in-plane bending loading.

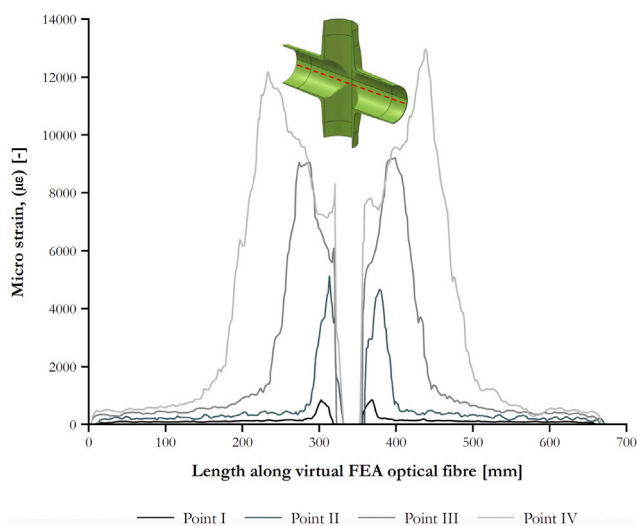


Fig. 28. FE prediction of longitudinal strain distributions under in-plane bending.

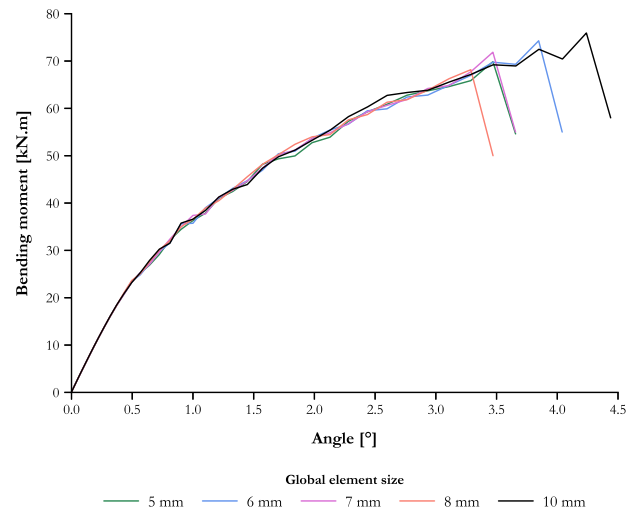


Fig. 29. Mesh-sensitivity for in-plane bending case with different global composite-wrap element sizes.

experimentally; however, the estimated debonding lengths are in close agreement between simulations and experiments, at approximately 300 mm.

The sensitivity of mesh to the adopted modelling strategy is assessed by repeating the analysis with global element sizes of 5, 6, 7, and 8 mm for the composite wrap, with mesh transitioning to 4 mm in the tapered regions in all cases and all other modelling parameters unchanged. The resulting moment–rotation responses are compared in Fig. 29. The curves are coincident throughout the nonlinear pre-peak regime, up to approximately 90% of the peak load of 10 mm mesh, with no variation in initial stiffness across the meshes. The peak moments vary between 68.4 kN m for 5 mm mesh and 75.9 kN m for 10 mm mesh, corresponding to a coefficient of variation of 4.3% without a monotonic dependence on element size. Since the peak load is governed by localized damage initiation at the crown, this scatter is attributed to differences in the local tetrahedral mesh topology at the critical region.

In all the models, composite crack initiation is predicted at the crown on the tensile side of bending, with propagation towards the saddle.

Based on the combined experimental and numerical results, a failure envelope for the wrapped composite joints is constructed, as shown in Fig. 30. For the numerical study, four additional simulations under combined axial tension and in-plane bending are performed to provide a more complete representation of the failure envelope predicted by the finite element model.

The results show that, under uniaxial tensile loading, the experimental and numerically predicted failure envelope point agree closely, indicating that the numerical model accurately captures the tensile failure capacity of the joints. However, when in-plane bending is applied, numerical simulations increasingly overpredict the joint capacity compared with experiments. Furthermore, the experimental results define an approximately linear failure envelope, whereas the finite element analysis predicts a distinctly nonlinear envelope. As only two

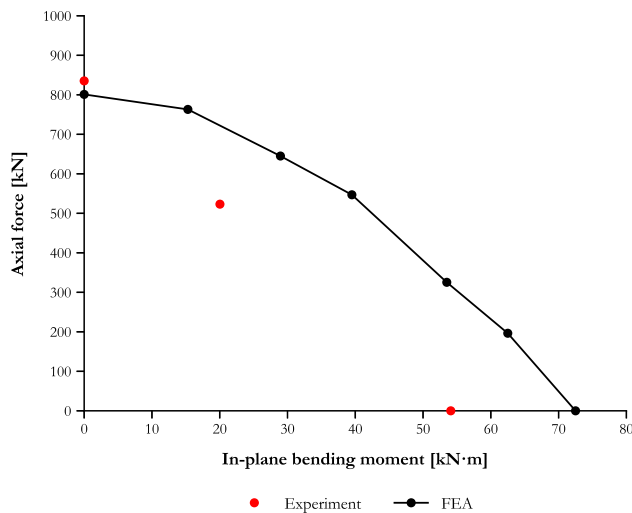


Fig. 30. Experimentally derived and numerically predicted axial tension and in-plane bending failure envelope for wrapped composite joints.

nominally identical specimens are tested per loading case, the experimental interaction represents an approximately linear trend for the present campaign rather than a generally applicable failure criterion. This discrepancy highlights the limitations of the current modelling approach, particularly in regimes dominated by bending-induced composite fracture and interfacial damage.

The overprediction observed under in-plane bending and combined loading conditions primarily arises from the use of Hill's anisotropic plasticity potential in the numerical modelling strategy. The Hill formulation is quadratic in stress and, while effective in representing in-plane anisotropy, is inherently convex. In wrapped composite joints subjected to bending combined loading, failure is governed by comparatively weak through-thickness properties of the composite laminate. These conditions induce stress states with significant out-of-plane normal and shear components, σ_{33} and τ_{13} , which are not adequately represented by a convex quadratic yield surface.

As a result, the Hill yield criterion is unable to capture the strongly non-elliptical and pinched yield envelope associated with matrix-dominated through-thickness deformation. This limitation leads to systematic overestimation of admissible stress states under bending, resulting in non-conservative predictions of both stiffness and strength. Enforcing concavity in the yield surface, as schematically illustrated in Fig. 31 (red curve), would require the use of coefficients that render the quadratic form indefinite. However, such modifications violate the assumptions of associated flow plasticity and are known to introduce numerical instability.

5. Conclusions

This study investigates the static fracture behaviour of X-shaped wrapped composite joints subjected to three distinct loading conditions. In total, six joints are tested experimentally. Advanced finite element models are developed and analysed under comparable loading conditions, and the numerical predictions are evaluated against experimental results to assess the modelling strategy's capability to capture composite fracture and its governing damage mechanisms. The following conclusions are drawn:

1. The composite fracture failure mode of thin wrapped composite joints remains consistent across all investigated loading scenarios, indicating that neither uniaxial tension, uniaxial in-plane

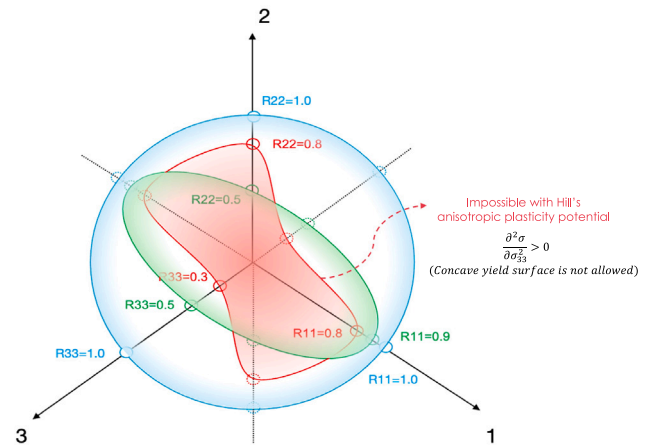


Fig. 31. Schematic illustration of yield-surface limitations of Hill's anisotropic plasticity model.

bending, nor combined axial-in-plane bending alters the fracture pattern, despite differences in the underlying stress states.

2. Composite fracture in joints does not occur in isolation but is consistently preceded by partial interfacial debonding. This coupled behaviour of progressive debonding and composite cracking is critical in providing ductility to the joint by enabling stable damage evolution prior to ultimate fracture.
3. The experimental load-deformation responses exhibit ductile behaviour for all loading conditions, with stable and visible crack initiation and propagation under bending-involved loading scenarios.
4. Under uniaxial tensile loading, the joints exhibit an ultimate resistance of 864 kN, with numerical predictions deviating by less than 10%. For combined axial tension and in-plane bending, the numerical simulations overpredict the experimental results by 21% in axial force and 37% in bending moment. Under uniaxial in-plane bending, the experiments reach an ultimate resistance of 54 kN m, while the numerical model predicts approximately 40% higher capacity.
5. The increasing deviation between numerical and experimental results in bending-dominated loading cases highlights the limitation of Hill's anisotropic plasticity potential in representing the weak through-thickness behaviour of composite materials, where fracture is governed by σ_{33} and τ_{13} .

Furthermore, quadratic formulation of Hill's anisotropic plasticity potential fails to capture interactions between stress components that may occur in wrapped composite joints, underscoring the need for alternative constitutive modelling strategies. Future studies will address the numerical overprediction under bending-involved loading through the development of a user-defined constitutive model that more accurately captures failure under combined stress states. In addition, as only two identical specimens are tested per loading case, a larger experimental programme is required to establish a statistically robust, generally applicable failure criterion for wrapped composite joints.

CRediT authorship contribution statement

Abishek Baskar: Writing – original draft, Visualization, Methodology, Investigation, Data curation, Conceptualization. **Mathieu Koetsier:** Writing – review & editing, Investigation. **Gisele Goes Cintra:** Writing – review & editing, Data curation. **Clement Waltener:** Investigation. **Marko Pavlovic:** Writing – review & editing, Supervision, Resources, Project administration, Funding acquisition, Conceptualization. **Frans van der Meer:** Supervision, Conceptualization.

Declaration of competing interest

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

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Data availability

Data will be made available on request.

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