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PII: S0020-7403(20)31088-2
DOI: https://doi.org/10.1016/j.ijmecsci.2020.105777
Reference: MS 105777

To appear in: International Journal of Mechanical Sciences

Received date: 12 March 2020
Revised date: 5 May 2020
Accepted date: 14 May 2020

Please cite this article as: Jazib Hassan, Ronan M. O’Higgins, Conor T. McCarthy, Nathalie Toso, Michael A. McCarthy, Mesoscale modelling of extended bearing failure in tension-absorber joints, International Journal of Mechanical Sciences (2020), doi: https://doi.org/10.1016/j.ijmecsci.2020.105777

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Mesoscale modelling of extended bearing failure in tension-absorber joints

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ABSTRACT

This paper presents the development and validation of a mesoscale composites damage model for predicting the energy absorption capability of “tension-absorber” joints. Tension-absorber joints are composite bolted joints specially designed to absorb energy in a crash via “extended bearing failure”, which involves the bolt forcing its way through the composite over a long distance. They have been proposed for use in future narrow-body composite fuselages. Here, extended bearing failure tests on a carbon fibre/epoxy laminate, are simulated using explicit three-dimensional finite element analysis. A physically based damage model is implemented in a user-defined subroutine. The model uses in-situ ply strengths, stress-based fibre failure criteria, Puck’s criteria for matrix damage, a nonlinear law for in-plane shear, a cohesive zone model for delamination, a crack band model to mitigate mesh sensitivity, and frictional contact between the pin and the laminate, and between adjacent plies once they delaminate. The model is found to accurately predict the global response, in terms of bearing strength, mean crush stress and energy absorption, and comparison with CT scans shows that it also captures the mesoscale damage very well. The model is used to predict the effects of pin diameter, laminate thickness and stacking sequence, and the results show excellent agreement with experimental findings.

Keywords: Bolted joints, damage modelling, crashworthiness, numerical validation

1. Introduction

Modern wide-body aircraft, like the Boeing 787 and Airbus A350, feature a fuselage made from carbon fibre reinforced polymer (CFRP) composites [1]. For such aircraft, the Federal Aviation Authority (FAA) raised a “Special Condition (SC)” [2, 3] to demonstrate an equivalent level of crash survivability to already certified comparable metallic aircraft. During the A350 design process, Airbus used numerical analysis as a “Means of Compliance”. Accurate global-scale failure models were developed after extensive experimental material and joints characterisation [4], demonstrating the importance of numerical models in the design and development of energy absorbing structures. Now, with increasing access to high performance computing, industry is looking for high-fidelity models which can go even further and deliver virtual testing [5], with an associated reduction in experimental testing. To be truly predictive under generic loading conditions, models must not only capture global behaviour such as force-displacement response but must also match the mesoscale damage evolution.

One of the most challenging problems one can set a composites damage model is bearing failure of composite joints, involving as it does, all the main composites failure modes. Recently Zhuang et al. [6] presented one of the first three-dimensional (3D) models to follow the bearing response of pin-loaded composites up to and beyond peak load (up to 0.25 mm beyond) and demonstrated excellent agreement with experiments for both the global and mesoscale response. In the current paper, a 3D model is developed and
applied to “extended bearing failure”. In this case, the aim is to follow the bearing response, far beyond peak load, with pin displacements up to 30 mm.

Mechanical joints are key components in fuselage structures and their behaviour has been studied extensively [7-14]. Recently, [15], DLR and Airbus investigated the potential use of joints as energy-absorbing devices in future narrow-body composite aircraft fuselages, to assist with meeting regulatory crashworthiness requirements. Due to the reduced available space compared to wide-body aircraft, not all the energy in a “foreseeable survivable impact event”, such as a 30 ft/s (9.14 m/s) vertical drop onto a hard surface, can be absorbed by sub-floor crush beams, so additional mechanisms are needed. The DLR/Airbus “tension-absorber” concept, illustrated in Figure 1(a), involves the modification of joints in areas such as the cargo and passenger crossbeams, which are loaded in tension as the fuselage deforms during impact. The modified joints would behave like normal joints under in-service loads, but in a crash would absorb considerable energy. The key design requirement is to prevent bolt pull-through or fracture, so that “extended bearing failure” occurs, resulting in the absorption of energy through crushing of the material in front of the bolt.

![Figure 1: (a) Tension-absorber Concept](image)

![Figure 1: (b) Pin-joint tests](image)

To examine the effects of individual material and geometric parameters, Airbus and DLR have studied a simplified version of the problem, namely a pin being pulled through a composite plate [18-21]. Recently [17], the current authors used this configuration to study the effects of pin diameter, laminate thickness and stacking sequence on bearing strength and energy absorption at quasi-static loading rates. The test rig used, and global response obtained, are illustrated in Figure 1(b). From a modelling standpoint, this recent study, [17], has the advantage that it employed IM7/8552 carbon fibre/epoxy, a material used in the third world-wide composites failure exercise [22], which has been extensively characterised in the literature [23-26]. Computed Tomography (CT) scans of interrupted tests were also performed, providing a detailed mapping of the internal damage and failure. Thus the results in [17] provide a useful dataset for testing numerical models on a very challenging problem.

There have been many finite element (FE) studies of composite bolted joints [27-43], with widely varying methods, in terms of model dimension (2D or 3D), element type (plane stress, shell, solid), FE solver (implicit or explicit), intra and interlaminar material failure criteria, damage propagation methods, and treatment of mesh
dependency issues, contact, friction, and geometric nonlinearity. The majority focus on global joint behaviour up to the point of bearing failure, with relatively few providing detailed comparisons of the mesoscale damage evolution with experiments. Apart from [6], the authors are not aware of any existing studies which have attempted to continue the analysis of bearing failure beyond the peak load.

In the current work an advanced 3D FE approach is developed for modelling the extended bearing failure mode illustrated in Figure 1(b). The aim is to predict the ultimate bearing strength (UBS), the mean crushing stress (MCS) after bearing failure, and the mass-specific energy absorption (SEA). The model needs to follow the joint response far beyond the peak load and be capable of predicting the change in response when material and geometric parameters are varied. In order to do so, it must represent the physical internal damage processes as faithfully as possible. It also has to remain numerically stable, at extreme levels of material damage, while adjusting contact conditions between the pin and the laminate, and within the laminate itself, as the simulation progresses. In line with several recent studies on composite joint modelling, [6, 33, 34], these stability requirements lead to the use of an explicit solver to avoid the convergence issues that plague implicit methods [40], even though the loading rate is quasi-static.

The approach taken is to extend the physically-based damage model of Egan et al. [10, 36, 44], which addressed bearing failure in normal (not tension-absorbing) countersunk bolted joints. That model included Puck’s criteria for matrix damage [45], a nonlinear law for in-plane shear, a maximum stress criterion for fibre failure, a crack band model to mitigate mesh sensitivity [46], and frictional contact between the fastener and the laminate. In the present work, the model is extended through the use of in-situ ply strengths, stress-based fibre failure criteria that incorporate fibre kinking effects in an efficient manner [47], a cohesive zone model (CZM) for simulating delamination, frictional contact between adjacent plies after they delaminate, and an efficient search algorithm for matrix damage identification. The model predictions are compared in detail to the test results in [17], at both the global and mesoscopic levels, and the fidelity and capability of the model to forecast the effects of variations in joint parameters are assessed.

2. Experimental set-up

A brief description of the experimental set-up is provided here, with full details available in [17]. The material used was HexPly® IM7/8552 (EU version: 134 gsm) unidirectional, continuous-fibre carbon fibre/epoxy composite prepreg (nominal ply thickness = 0.125 mm). Specimens with the geometry shown in Figure 2 were extracted from panels manufactured in an autoclave using water jet cutting. As outlined in Table 1, fifteen configurations were tested. All layups were quasi-isotropic, but the stacking sequence varied, as did the pin diameter and laminate thickness. The 2 mm and 3 mm thick specimens are labelled “dispersed” or “blocked” depending on whether the stacking sequence is $[45/-45/90]_m$ with $n = 2, 3$, or $[45_m/-45_m/90_m/0_m]_m$ with $m = 2, 3$, respectively. The configuration code indicates the stacking sequence, pin diameter and laminate thickness (e.g. DS_D4_T2 or BK_D12_T3). DS/BK is omitted for the 1 mm thick specimens, since $[45/-45/90]_m$ could be considered the “root” stacking sequence of the dispersed test series (with $n = 1$), or the blocked test series (with $m = 1$). There are five different stacking sequences in total. The focus is on layups with $0^\circ$, $\pm45^\circ$ and $90^\circ$ ply orientations because these make up the vast majority of layups used on aircraft. An additional study is planned in future to look at less conventional layups, including plies with $22.5^\circ$, $30^\circ$, $60^\circ$ and $67.5^\circ$ orientations.

Each test consisted of pulling a steel pin through the laminate at quasi-static loading speed (1.67x10$^{-4}$ m/s), using the specialised rig illustrated in Figure 1(b). The reaction force was obtained by the load cell of the servo-
hydraulic test machine and the pin displacement was measured by a digital image correlation method. Four repeats of each configuration were tested, with further interrupted tests undertaken for CT analysis.

Figure 2 Composite specimen dimensions. Thicknesses tested were 1 mm, 2 mm and 3 mm.

Table 1: Test configurations. Variables are stacking sequence, pin diameter, and laminate thickness. DS_D4_T2 means dispersed stacking sequence, 4 mm pin and 2 mm thickness. BK stands for blocked stacking sequence.

| Configuration | Code  | Stacking sequence | Pin diameter, D (mm) | Laminate thickness, t (mm) | D/t |
|---------------|-------|-------------------|----------------------|---------------------------|-----|
| 1             | D4_T1 | [45/-45/90/0],    | 4                    | 1                         | 4   |
| 2             | D8_T1 | [45/-45/90/0],    | 8                    | 1                         | 8   |
| 3             | D12_T1| [45/-45/90/0],    | 12                   | 1                         | 12  |
| 4             | DS_D4_T2| [45/-45/90/0],  | 4                    | 2                         | 2   |
| 5             | DS_D8_T2| [45/-45/90/0],  | 8                    | 2                         | 4   |
| 6             | DS_D12_T2| [45/-45/90/0], | 12                   | 2                         | 6   |
| 7             | DS_D4_T3| [45/-45/90/0],   | 4                    | 3                         | 1.33|
| 8             | DS_D8_T3| [45/-45/90/0],  | 8                    | 3                         | 2.66|
| 9             | DS_D12_T3| [45/-45/90/0], | 12                   | 3                         | 4   |
| 10            | BK_D4_T2| [45/-45,90,0],  | 4                    | 2                         | 2   |
| 11            | BK_D8_T2| [45/-45,90,0],  | 8                    | 2                         | 4   |
| 12            | BK_D12_T2| [45/-45,90,0], | 12                   | 2                         | 6   |
| 13            | BK_D4_T3| [45,-45,90,0],   | 4                    | 3                         | 1.33|
| 14            | BK_D8_T3| [45,-45,90,0],  | 8                    | 3                         | 2.66|
| 15            | BK_D12_T3| [45,-45,90,0], | 12                   | 3                         | 4   |

A typical response, illustrated in Figure 1(b), exhibited a peak force at bearing failure, followed by a load drop for 5-10 mm, and a transition to a relatively constant crushing force which persisted until the pin exited the end of the laminate. To allow comparisons on a material level, the following performance parameters are defined. The ultimate bearing strength (UBS), σ_{ult} is defined in accordance with ASTM standard D 5961/D 5961M [48]:

$$\sigma_{ult} = \frac{F_{max}}{D \cdot t}$$

(1)

where D is the pin diameter and t is laminate thickness. The mean crushing stress (MCS) is:

$$\sigma_{mean} = \frac{F_{mean}}{D \cdot t}$$

(2)

where $F_{mean}$ is calculated between 5 mm and 30 mm pin displacement. Finally the mass-specific energy absorption (SEA) is defined as:
where \( m_{\text{absorbed}} \) is the mass of the material involved in energy absorption, \( \rho \) is material density and \( s_m \) is taken here as 30 mm. In [17], \( s_m \) was taken to be 40 mm, but the shorter distance is used here for comparison with the models, due to considerations of computation time. The difference between the SEA calculated over 30 mm or 40 mm is very small and running the simulations beyond 30 mm provides little additional value. The factor of 1.2 in equation (3) is based on an estimation in [19] that, for materials with brittle fibres, the width of destroyed material is 20% larger than the pin diameter.

3. Composite damage model

3.1 3D elastic behaviour and nonlinear shear law

A unidirectional (UD) ply damage model has been implemented in an Abaqus/Explicit VUMAT subroutine.

Integration point stresses, \( \sigma_{i^{+\Delta t}} \) are updated based on total strains \( \varepsilon_{i^{+\Delta t}} \) at the current time increment, using equation (4), where indices 1, 2, and 3 refer to the fibre, in-plane transverse and through-thickness directions respectively.

\[
\begin{bmatrix}
\sigma_{1^{+\Delta t}} \\
\sigma_{2^{+\Delta t}} \\
\sigma_{3^{+\Delta t}} \\
\tau_{12^{+\Delta t}} \\
\tau_{23^{+\Delta t}} \\
\tau_{31^{+\Delta t}}
\end{bmatrix} =
\begin{bmatrix}
E_{11}A(1-v_2v_3) & E_{12}A(v_2-v_3) & E_{12}A(v_3-v_2) & 0 & 0 & 0 \\
E_{22}A(1-v_2v_3) & E_{22}A(v_2-v_3) & E_{22}A(v_3-v_2) & 0 & 0 & 0 \\
E_{33}A(1-v_2v_3) & E_{33}A(v_2-v_3) & E_{33}A(v_3-v_2) & 0 & 0 & 0 \\
E_{12}A(1-v_3v_1) & E_{12}A(v_3-v_1) & E_{12}A(v_1-v_3) & 0 & 0 & 0 \\
E_{23}A(1-v_3v_1) & E_{23}A(v_3-v_1) & E_{23}A(v_1-v_3) & 0 & 0 & 0 \\
E_{31}A(1-v_3v_1) & E_{31}A(v_3-v_1) & E_{31}A(v_1-v_3) & 0 & 0 & 0
\end{bmatrix}
\]

(4)

where:

\[
A = \frac{1}{1-v_3v_1-v_2v_3-v_1v_2-2v_1v_2v_3}
\]

(5)

and \( E_{11}, E_{22}, E_{33} \) are the longitudinal, transverse and through-thickness Young’s Moduli respectively, \( v_{12}, v_{13}, v_{21}, v_{23}, v_{31}, v_{32} \) are the Poisson’s ratios, and \( G_{12}, G_{23}, G_{31} \) are the three shear moduli.

To implement nonlinear shear behaviour, the maximum shear strain over time, \( \bar{\gamma}_{12} = \max \left| \gamma_{12}(t) \right| \) is monitored and, as in [49, 50], decomposed into elastic (\( \gamma' \)), elastic-damage (\( \gamma^{ed} \)) and inelastic (\( \gamma^{in} \)) parts:

\[
\bar{\gamma}_{12} = \gamma'_{12} + \gamma^{ed}_{12} + \gamma^{in}_{12}
\]

(6)

where:

\[
\gamma'_{12} = \frac{f_{\tau_{12}}}{G_{12}}, \quad \gamma^{ed}_{12} = \frac{f_{\tau_{12}}d_{12}}{G_{12}(1-d_{12})}
\]

(7)

The damage variable \( d_{12} \) controls the reduction of the original shear modulus \( G_{12}^0 \) due to progressive matrix damage occurring under shear loading. Figure 3. Following the method of Donadon et al. [50], it varies with \( \bar{\gamma}_{12} \) according to equation (8), where \( a \) is the slope of \( G_{12}/G_{12}^0 \) versus \( \bar{\gamma}_{12} \) (the “gradual stiffness reduction curve”), which is determined experimentally.

\[
d_{12} = -a\bar{\gamma}_{12}
\]

(8)

Inelastic strain is determined as \( \gamma^{in} = \bar{\gamma}_{12} - \gamma'_{12} - \gamma^{ed}_{12} \) and in-plane shear stress is updated as:
\[
\tau_{12} = \begin{cases} 
\frac{\bar{\gamma}_{12}}{G_{11}} f_{11} & \text{if } \bar{\gamma}_{12} < \bar{\gamma}_{12} \\
\frac{G_{11}}{G_{11}} (1 - d_{12})(\gamma_{12} - \gamma_{12}^0) & \text{if } \bar{\gamma}_{12} \ge \bar{\gamma}_{12}
\end{cases}
\tag{9}
\]

The parameter \( f_{112} \) defines the shape of the nonlinear response. The IM7/8552 carbon fibre/epoxy used here shows a response similar to that shown in Figure 3, and the curve is fitted using two cubic polynomials, one applicable up to a shear strain of \( \gamma_{PL,max} \) (see Figure 3), the other applicable above \( \gamma_{PL,max} \), i.e.:

\[
f_{11} = \begin{cases} 
ce_i \bar{\gamma}_{12}^3 + c_2 \bar{\gamma}_{12}^2 + c_3 \bar{\gamma}_{12} & \text{if } \bar{\gamma}_{12} \leq \gamma_{PL,max} \\
d_1 \bar{\gamma}_{12}^3 + d_2 \bar{\gamma}_{12}^2 + d_3 \bar{\gamma}_{12} + d_4 & \text{if } \bar{\gamma}_{12} > \gamma_{PL,max}
\end{cases}
\tag{10}
\]

where \( c_i \) and \( d_i \) (\( i = 1, 2, 3 \)) are the coefficients of the fitted curve and \( d_4 \) is the shear stress at \( \gamma_{PL,max} \). A feature of the model, previously developed by Egan et al. [44], is a purely symmetric shear stress–strain law on load reversal. This aspect is discussed in detail in [44].

Figure 3: IM7/8552 non-linear shear behaviour, fitted with two third-order polynomials, \( f_{112,1} \) and \( f_{112,2} \). \( \bar{\gamma}_{12} \) is maximum shear strain over time, \( \gamma' \), \( \gamma^{ed} \), and \( \gamma^m \) are elastic, elastic-damage, and inelastic parts, respectively.

3.2 Fibre failure

In line with previous researchers [51–56], for tensile fibre failure, a simple maximum stress criterion is used:

\[
\text{For } \sigma_{11}^{\text{tensile}} > 0: \quad f_p = \frac{\sigma_{11}^{\text{tensile}}}{X_t} \ge 1
\tag{11}
\]

where \( X_t \) is the tensile fibre-direction strength. For compressive fibre failure, some researchers use a fibre-kinking model [6, 57–59], on the basis that longitudinal compressive failure is caused by the formation of fibre kink bands following degradation of the supporting matrix. The model of Pinho et al. [59] for example, successfully predicted the increase in compressive strength due to hydrostatic pressure shown experimentally in [60]. Fibre-kinking models are however, computationally expensive, and Egan et al. [44] justified using a simple maximum stress criterion instead (\( f_p = -\sigma_{11}^{\text{tensile}}/X_c \ge 1 \) on the basis that \( X_c \) measured in a standard compression test would be lower than the in-situ compressive strength in the torqued-up countersunk joints in [44], so predictions would be conservative. Unfortunately in the current pin-loaded specimens, no torque exists, so this argument cannot be made. However, Raimondo et al. [47] derived a fibre compression failure criterion from polynomial fitting of experimental failure envelopes. They proposed that longitudinal and shear stresses contribute to shear fracture of fibres, followed by fibre rotation and in-plane matrix shearing at the crack tip, which in turn promotes kink band development [61]. The failure criteria for quasi-static loading was given as:
For $\sigma_{11}^{+M} > 0$: 
\[ f_{\mu} = \frac{\sigma_{11}^{+M}}{X_{\mu}} + \left( \frac{t_{12}^{+M}}{S_{12}} \right)^{2} + \left( \frac{t_{13}^{+M}}{S_{13}} \right)^{2} \geq 1 \]  
(12)

where $X_{\mu}$, $S_{12}$ and $S_{13}$ are the quasi-static compressive fibre, in-plane shear and out-of-plane shear strengths respectively. This fitted experimental data well for $\alpha \geq 1$. Given the high level of detail in the other parts of our model, and the computational expense of including an explicit fibre kinking model, the approach of using equation (12) was followed instead ($\alpha$ is selected here to be 2.5). Although this is a compromise it is still an improvement over the criterion used in [44].

3.3 Matrix failure

To predict matrix failure, the stress tensor is rotated about the 1-direction by variable angle $\phi$, $0 \leq \phi \leq \pi$, as shown in Figure 4(a), to test potential fracture planes. The tractions on these planes (dashed quantities) are used to evaluate the Puck-Schürmann failure criteria [45]:

\[ \begin{align*}
\text{For } \sigma_{22}^{+M} > 0: & \quad f_{\mu} = \left( \frac{\sigma_{22}^{+M}}{Y_{t}} \right)^{2} + \left( \frac{t_{12}^{+M}}{S_{12}} \right)^{2} + \left( \frac{t_{23}^{+M}}{S_{23}} \right)^{2} \geq 1 \\
\text{For } \sigma_{22}^{+M} < 0: & \quad f_{\mu} = \left( \frac{\sigma_{22}^{+M}}{S_{12} - \mu \sigma_{11}^{+M}} \right)^{2} + \left( \frac{t_{12}^{+M}}{S_{12} - \mu \sigma_{11}^{+M}} \right)^{2} \geq 1
\end{align*} \]

(13)

where $Y_{t}$ is the transverse tensile strength, $S_{12}$ is the longitudinal shear strength, and $S_{23}$ is the transverse shear strength. The transverse friction coefficient $\mu_{t}$ is defined using Mohr-Coulomb theory as:

\[ \mu_{t} = \frac{1}{\tan(2\phi_{0})} \]

(14)

where $\phi_{0}$ is the fracture surface orientation for pure transverse compressive failure which typically has a value of about $53^\circ$ for unidirectional polymer matrix composites. The longitudinal friction coefficient is given by:

\[ \mu_{l} = \mu_{t} \frac{S_{12}}{S_{23}} \]

(15)

The angle at which failure is detected, $\phi$, determines the orientation of the fracture surface. This search process can be computationally expensive, so to efficiently find the fracture surface a "Golden Search Algorithm", as outlined in [62], was implemented.

Figure 4: (a) Tractions acting on potential matrix fracture plane obtained by rotating through angle $\phi$ about the 1-direction, (b) illustration of crack band model implementing nonlinear softening law of eq. (17)
3.4 Crack band model and definition of characteristic lengths

To mitigate mesh sensitivity, a crack-band model [46] is used to evolve the damage variables \( d_p, d_c, d_m \) and \( d_{mm} \) from 0 at failure initiation, to 1 at complete degradation. The final failure strain, \( \varepsilon' \), is adjusted based on the characteristic element length, \( L \):

\[
\varepsilon' = \frac{2G}{\sigma^o L}
\]

where \( G \) is the fracture energy and \( \sigma^o \) is the stress at failure onset. Damage variable growth is described by [59]:

\[
d^{i+M} = \max\left(0, d'\right), \min\left\{1, 1 - \frac{\varepsilon'^o}{\varepsilon' - \varepsilon'^o} \left[1 + \kappa^2 \left(2\kappa - 3\right)\right]\right\}, \quad \text{where} \quad \kappa = \frac{\varepsilon^{i+M} - \varepsilon'^o}{\varepsilon' - \varepsilon'^o}
\]

which causes nonlinear softening of stress components, as illustrated in Figure 4(b). Since damage is irreversible, the damage variables are only updated by increasing values.

For predicting fibre damage, the failure onset stress \( \sigma^o \) corresponds to the fibre direction strength, and \( \varepsilon^o \) and \( \varepsilon' \) also refer to 1-direction components. Predicting matrix damage is more complex, since several tractions can promote failure, and so an equivalent stress, \( \sigma_{m}^{o} \), and equivalent strain, \( \varepsilon_{m}^{e} \), must be defined:

\[
\sigma_{m}^{o} = \sqrt\left(\sigma_{2}^{+}\right)^2 + \left(\sigma_{12}^{+}\right)^2 + \left(\sigma_{1}^{+}\right)^2 \quad \text{where} \quad \sigma_{2}^{+} = \frac{d^{i+M}}{d_{mm}^{i+M}} = 0
\]

\[
\varepsilon_{m}^{e} = \sqrt\left(\varepsilon_{2}^{+}\right)^2 + \left(\varepsilon_{12}^{+}\right)^2 + \left(\varepsilon_{1}^{+}\right)^2 \quad \text{where} \quad \varepsilon_{2}^{+} = \frac{d^{i+M}}{d_{mm}^{i+M}} = 0
\]

where \( \langle x \rangle = \max(0, x) \) and stress and strain components are obtained from the stress tensor rotated by the fracture plane angle, \( \theta \). As in [59], the strain tensor prior to rotation contains elastic in-plane shear strain. This ensures that only elastic internal energy contributes to energy absorption associated with localised fracture. The strain measure used to grow the matrix damage variable (\( \varepsilon_{m}^{i+M} \)) after the damage onset is given in equations (20) and (21), depending on whether failure is tensile or compressive.

For \( f_m \geq 1 \):

\[
\varepsilon_{m}^{i+M} = \sqrt\left(\varepsilon_{2}^{+}\right)^2 + \left(\varepsilon_{12}^{+}\right)^2 + \left(\varepsilon_{1}^{+}\right)^2 \quad \text{where} \quad \varepsilon_{2}^{+} = \frac{d^{i+M}}{d_{mm}^{i+M}} > 0
\]

For \( f_m \geq 1 \):

\[
\varepsilon_{m}^{i+M} = \sqrt\left(\gamma_{1}^{+}\right)^2 + \left(\gamma_{2}^{+}\right)^2 + \left(\gamma_{3}^{+}\right)^2 \quad \text{where} \quad \gamma_{2}^{+} = \frac{d^{i+M}}{d_{mm}^{i+M}} > 0
\]

To calculate the final failure strains for fibre and matrix damage modes, the following expressions are used based on the crack band model [46]:

For \( f_p \geq 1 \):

\[
\varepsilon_{p}^{f} = \frac{2G_p}{X_p L_f}
\]

For \( f_c \geq 1 \):

\[
\varepsilon_{c}^{f} = \frac{2G_c}{X_c L_f}
\]

For \( f_m \geq 1 \):\[
\varepsilon_{m}^{f} = \frac{2G_m}{\sigma_{m}^{o} L_m}
\]

For \( f_m \) or \( f_m \geq 1 \): \[
\varepsilon_{m}^{f} = \frac{2G_m}{\sigma_{m}^{o} L_m}
\]
where $G_f$ and $G_c$ are the fracture energies associated with tensile and compressive fibre failure respectively, and $L_f$, $L_c$ are the associated characteristic lengths. The fracture energy for matrix failure ($G_m$) for any mixed-mode failure scenario is computed as:

$$G_m = G_k \left( \frac{r^{k+\Delta k}}{\sigma_m} \right)^2 + G_h \left( \frac{r^{h+\Delta h}}{\sigma_m} \right)^2 + G_l \left( \frac{r^{l+\Delta l}}{\sigma_m} \right)^2$$  \hspace{1cm} (25)

where $G_k$ and $G_h$ are the transverse fracture energies, and the stress components are the tractions at the onset of matrix failure.

The characteristic element length is provided by ABAQUS to VUMAT routines as the variable “charLength”. However, this value is only appropriate for models with cubic elements in which fracture is perpendicular to an element side. In simulations of bearing failure, near-hole elements are inevitably of non-cubic shape and non-standard orientation, while the ply orientations and Mohr–Coulomb material behaviour promote cracks which are angled with respect to element sides. Hence instead of using “charLength” an approach developed by Egan et al. [44] is used. A python script pre-processes the mesh to compute element geometries from nodal coordinates. These are used to compute characteristic lengths, $L_f$ and $L_c$, which accurately account for crack orientation, element orientation and element shape. Full details are given in [44].

3.5 Damage enforcement and element deletion

In general, damage variables, $d$, are used to soften the effective stress, $\sigma^d$, to yield the applied/Cauchy stress, $\sigma^d = \sigma^d (1 - d)$. Here, for simulation of ply damage, the stress tensor must be softened. If matrix damage is detected, the elastic stress tensor is rotated by angle $\phi \rightarrow \phi$ to the fracture plane, giving $\sigma^{t+M}$, and then the tractions acting on this plane are softened using equation (26), yielding $\tau^{t+M}$. The normal traction $\sigma^{t+M}$ is only softened in the case of tensile matrix damage since cracks will close upon load reversal. A rotation by $-\phi \rightarrow \phi$ back to the ply coordinate system gives $\sigma^{t+M}$.

$$\sigma^{t+M} = \begin{bmatrix}
\sigma_{11}^{t+M} \\
\sigma_{22}^{t+M} \left(1 - d_{f}^{t+M}\right) \\
\sigma_{33}^{t+M} \\
\tau_{12}^{t+M} \left(1 - d_{m}^{t+M}\right) \\
\tau_{23}^{t+M} \left(1 - d_{m}^{t+M}\right) \\
\tau_{31}^{t+M}
\end{bmatrix} \quad (26)$$

When fibre failure is detected, all stress tensor components are softened according to equation (27), on the assumption that fibre rupture damages the supporting matrix. The resulting Cauchy stress tensor, $\sigma^{t+M}$, is provided to Abaqus:

$$\sigma^{t+M} = \sigma^{t+M} \left(1 - d_{f}^{t+M}\right) \left(1 - d_{fc}^{t+M}\right) \quad (27)$$
Excessive element distortion can cause explicit simulations to abort, so element deletion criteria are defined in equations (28) and (29). After extensive trial simulations, $\Omega_1$ and $\Omega_2$ were set to be 0.8.

$$f_{\text{erosion,1}} = \max \left( \text{abs}(\gamma_1^{\text{e,1}}), \text{abs}(\gamma_2^{\text{e,1}}) \right) - \Omega_1; \quad f_{\text{erosion,2}} \geq 0$$  \hspace{1cm} (28)

$$f_{\text{erosion,2}} = \text{abs}(\gamma_1^{\text{e,2}}) - \Omega_2; \quad f_{\text{erosion,2}} \geq 0$$  \hspace{1cm} (29)

### 3.6 Interlaminar damage

The uncoupled, mixed-mode cohesive zone model (CZM), already implemented in ABAQUS, is used to model interlaminar failure [63]. The CZM follows a bi-linear traction-separation law with linear softening for opening (mode I) and shearing (mode II and III) modes, as shown in Figure 5(a) and (b). The initial linear behaviour is defined by:

$$\sigma = \begin{bmatrix} \sigma_I \\ \sigma_{II} \\ \sigma_{III} \end{bmatrix} = \begin{bmatrix} K_{nn} & K_{ns} & K_{ns} \\ K_{ns} & K_{ss} & K_{ss} \\ K_{ns} & K_{ss} & K_{ss} \end{bmatrix} \begin{bmatrix} \delta_I \\ \delta_{II} \\ \delta_{III} \end{bmatrix}$$  \hspace{1cm} (30)

where $\sigma_I$, $\sigma_{II}$ and $\sigma_{III}$ are the normal, mode II shear and mode III shear tractions respectively, $K$ is the stiffness matrix, and $\delta_I$, $\delta_{II}$ and $\delta_{III}$ are the normal, mode II shear and mode III shear separations respectively.

A suitable approximation of the stiffness of a thin cohesive layer is provided by the bulk elastic properties, i.e. $K_{nn} = E_{11}$, $K_{ss} = K_{ss} = E_{II}$, while the off-axis terms in $K$ can be ignored for elastic behaviour which is uncoupled between normal and shear components [64]. Damage is initiated using a quadratic nominal stress criterion:

$$\left( \frac{\sigma_I}{\sigma_I^0} \right)^2 + \left( \frac{\sigma_{II}}{\sigma_{II}^0} \right)^2 + \left( \frac{\sigma_{III}}{\sigma_{III}^0} \right)^2 = 1$$  \hspace{1cm} (31)

where the Macauley bracket $\{ \}$ is used to ensure that a purely compressive stress state does not initiate damage, and $\sigma_I^0$, $\sigma_{II}^0$ and $\sigma_{III}^0$ are the critical traction values. Once this criterion is satisfied, damage initiates and the stiffness decreases progressively. The Benzeggagh-Kenane (BK) law [65] is used to propagate the delamination:

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Figure 5: Traction-separation law, defining the (a) normal and (b) mode II shearing behaviour of cohesive elements. Mode III shearing law is the same as for Mode II.
where $G_i'$, $G_{ii}'$ and $G_{iii}'$ are the critical fracture energies in the normal and shear directions, while $\eta$ is a cohesive property parameter.

### 3.7 Input parameters for the VUMAT

UD carbon fibre/epoxy IM7/8552 has been used by many researchers and has been fully characterised at various loading rates [23-26, 66-69]. The elastic and damage properties at quasi-static loading rates are summarised in Table 2 to Table 7. In [6] it was proposed that *in-situ* ply strengths should be used in the simulation of pin-bearing problems, and comparisons with experiments validated this approach. As shown in Table 5, transverse and shear strengths were made dependent on the location and thickness of a ply e.g. thin outer ply ($to$), thin embedded ply ($te$) or thick ply ($thick$).

#### Table 2 IM7/8552 elastic properties [23]

| $E_{11}$ | $E_{22}$ | $E_{33}$ | $v_{12}$ | $v_{13}$ | $v_{23}$ | $G_{12}$ | $G_{13}$ | $G_{23}$ |
|----------|----------|----------|----------|----------|----------|----------|----------|----------|
| (GPa)    | (GPa)    | (GPa)    |          |          |          | (GPa)    | (GPa)    | (GPa)    |
| 71.4     | 0.08     | 0.08     | .32      | .32      | .5       | 29.0     | 0.02     | 29.0     |

* Calculated assuming transverse isotropy.

#### Table 3 IM7/8552 shear stress-strain curve fitting parameters for equation (10) [26]

| $c_1$ | $c_2$ | $c_3$ | $d_1$ | $d_2$ | $d_3$ | $d_4$ |
|-------|-------|-------|-------|-------|-------|-------|
| $8.453 \times 10^4$ | $-1.441 \times 10^4$ | $6.178 \times 10^4$ | $-4.423 \times 10^4$ | $2.918 \times 10^4$ | $-1.909 \times 10^4$ | $8.642 \times 10^4$ |

#### Table 4 IM7/8552 strength properties [23, 66]

| $X_1$ | $X_e$ | $Y_1$ | $Y_e$ | $S_{12}$ | $S_\sigma$ |
|-------|-------|-------|-------|----------|-----------|
| (GPa) | (GPa) | (MPa) | (MPa) | (MPa)    | (MPa)     |
| 2.1   | 1.0   | 62    | 19    | 92       | 75        |

#### Table 5 IM7/8552 in-situ shear and transverse tension strengths [70, 71]. Single plies are “thin”, blocked plies (two or more plies with same orientation) are “thick”. Strengths depend on whether the ply is a thin outer ply ($to$), a thin embedded ply ($te$), or a thick ply ($thick$).

| $S_{12to}$ | $S_{12te}$ | $S_{12thick}$ | $Y_{to}$ | $Y_{te}$ | $Y_{thick}$ |
|------------|------------|---------------|----------|----------|------------|
| (MPa)      | (MPa)      | (MPa)         | (MPa)    | (MPa)    | (MPa)      |
| 10         | 13         | 113           | 16       | 10       | 98.        |
| 7          | 0.2        | 1             | 0.2      | 1.4      | 7          |

#### Table 6 IM7/8552 damage properties [24, 66, 72], for equations (15), (22), (23) and (25)

| $G_{u}$ | $G_{fc}$ | $G_{fc}$ | $G_{fee}$ | $\mu_l$ | $\mu_e$ |
|---------|----------|----------|-----------|---------|---------|

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Table 7 IM7/8552 interface properties for 0/0 interface [73-75]

| $\sigma_i^0$ (MPa) | $\sigma_{ii}^0 = \sigma_{iii}^0$ (MPa) | $E_i$ (N/m$^2$) | $E_{ii} = E_{iii}$ (N/m$^2$) | $G_i^0$ (kJ/m$^2$) | $G_{ii}^0$ (kJ/m$^2$) | $\eta$ |
|------------------|---------------------------|-----------------|-----------------|-----------------|-----------------|------|
| 60               | 90                        | 4.67x10$^{14}$  | 1.67x10$^{14}$  | 0.6             | 0.6             | 0.3  |

3.8 Model details

The pin-bearing tests were modelled in ABAQUS/Explicit. Meshing was performed as shown in Figure 6, using a python script for consistency among the various pin diameters and laminate thicknesses. Three-dimensional, reduced integration, 8-noded solid elements (C3D8R) were used. In displacement-based non-linear FE models, the use of full integration elements over-estimates the stiffness matrix and the elements may suffer from shear locking. To mitigate this effect, the use of reduced integration elements (C3D8R) is recommended. Furthermore, using these elements lowers the computational cost [63]. One downside of using reduced integration elements is that a fine mesh must be used near structural boundaries to capture the stress concentrations at such locations [63, 76]. However, this disadvantage was more than compensated for by the advantages listed above for the current problem. These elements are also prone to hourglassing due to the presence of several spurious zero-energy modes, so to ameliorate these effects “pure stiffness” section control was applied [63]. For interface elements, an 8-noded three-dimensional cohesive elements (COH3D8) were used, which had a thickness of six microns. This element type is typically used to capture delamination behaviour in composite structures [6, 77]. With these elements, it is assumed that the cohesive layers are only subjected to direct through-thickness ($\varepsilon_{33}$) strains and transverse shear strains ($\varepsilon_{23}$, $\varepsilon_{13}$). The other two direct strains, $\varepsilon_{11}$, $\varepsilon_{22}$ and shear strain $\varepsilon_{12}$ are assumed to be zero. These assumptions are appropriate in situations where a relatively thin and compliant cohesive layer bonds two relatively rigid parts [63].

Each ply was modelled with a single element through the thickness. To accurately predict bearing strength, a variable element size was used over the first 2.5 mm of crushing length (from 0.3125 mm × 0.2 mm to 0.3125 mm × 0.5 mm), see Figure 6. The rest of the crushing length had a constant element size (0.5 mm × 0.5 mm), as can be seen in Figure 6. The reason for selecting such a fine mesh near the hole edge is to capture the stress concentration at the pin-hole contact effectively using the chosen reduced integration elements. In the material model, the crack band model is implemented to ameliorate mesh sensitivity, as discussed in section 3.4. Elastic, isotropic material properties were used for the hardened steel pins (E = 210 GPa, v = 0.25), while the laminate was modelled with elastic properties in the grip region, where no damage was expected, and via the VUMAT described in the previous sections, in regions predicted to damage, see Figure 7(a). Non-linear geometrical effects were included due to the finite strains developed during crushing of the elements.
Contact between the laminate and pin was defined using the “general contact” algorithm [63] with the choice of friction co-efficient discussed in section 4.1. Continuous laminate crushing results in deletion of elements, exposing new surfaces for contact with the pin. To ensure continuity of the simulation, the contact surface needs to be updated by creating interior surfaces. ABAQUS/CAE does not currently support the creation of interior surfaces, so the input files had to be modified manually. Firstly an element set was defined containing elements lying in the crushing zone, see Figure 6, then interior surfaces were created using the *SURFACE command. Additional contact pairs were then defined in the ABAQUS input file using these surfaces.

Figure 6: FE mesh of steel pin and composite plate showing variable element size in immediate vicinity of hole and element set used for creation of interior surfaces

The leftmost (as per Figure 7(b)) 45 mm of the laminate was given a fixed boundary condition (imitating the clamps of the fixed end of the test machine), while a velocity boundary condition was applied to the centreline of the pin (see Figure 7(b)). To aid in achieving an efficient quasi-static solution, and to provide an accurate and repeatable measure of the peak force and bearing strength, a sigmoid function was used to increase the velocity gradually from zero to the applied velocity. After this, the velocity was kept constant.

Figure 7: (a) Isometric view of FE model assembly showing different material sections, (b) side view of FE model assembly showing applied boundary conditions, and composite/interface elements

In terms of computational cost, the developed model can be quite expensive especially for quasi-static loading. The stable time increment can be increased by increasing the smallest element size, increasing the velocity, or by applying mass scaling [78]. The smallest element size is difficult to alter due to the use of
cohesive elements and one element per ply in the thickness direction. It was decided not to use mass scaling based on past experience. To examine the feasibility of using an increased velocity, a series of simulations were performed and the kinetic to internal energy ratio was examined until the peak load, as this is important for quasi-static analysis with an explicit solver [78]. Three loading rates were trialled on a BK_D4_T2 configuration (see Table 1), 0.1 m/s, 0.5 m/s and 1 m/s. Note that no strain-rate dependency was included in the material model in the VUMAT, so any change in behaviour is not due to material properties. The numerical reaction force was calculated from the fixed end of the laminate while the displacement was extracted from the centreline of the pin. Reaction force is plotted against pin displacement for the different loading rates in Figure 8(a), and the ratio of kinetic to internal energy in each model is given in Figure 8(b). As a rule of thumb, the kinetic energy should not exceed 5-10% of the internal energy for a quasi-static solution [63]. In the 1 m/s model, the ratio of kinetic-to-internal energy exceeds 15% early in the analysis, indicating a quasi-static solution is not achieved. The load-deflection response for the other loading rates converge reasonably well onto one response, Figure 8(a), and the energy ratio is well within bounds. Based on this trial study, 0.5 m/s was used in all the quasi-static analyses, as it gave good agreement with experimental data without showing an detrimental increase in kinetic-to-internal energy ratio, and it was also computationally efficient. Simulations were run on a single node of a high-performance computing system, where each node has 2 x 20-core 2.4 GHz Intel Xeon Gold 6148 (Skylake) processors.

Figure 8: Effect of loading velocity on (a) load-deflection and (b) ratio of kinetic energy to internal energy

4. Results and Discussion

4.1 Model calibration

In this section, the model is calibrated in three iterations. In iteration 1, the contact conditions and friction coefficients are chosen. In iteration 2, a deeper analysis of the fracture energies used for the cohesive elements between plies is undertaken. In iteration 3, a simplification is made in the interests of saving computational time.

The experimental study, [17], showed that delamination plays an important role in the joint response. Delamination is modelled here with the in-built ABAQUS bi-linear traction-separation law, as explained in Section 3.6. Now, as the bearing load increases, cohesive elements between the plies start to delete. Once that happens, if contact is not defined between the plies, they are free to pass through each other, which is non-physical. Thus, we defined contact between plies via interior contact between ply elements. Having done this, friction coefficients between the pin and the laminate, and between the plies themselves, have to be chosen. Iteration 1 addresses these issues, with configuration BK_D4_T2 used as a sample case.

Figure 9(a)-(d) show the effect of these friction parameters on the global and mesoscopic response. The stacking sequence is colour-coded for ease of interpretation and dashed and dotted lines are used to indicate the
average experimental UBS and MCS values respectively. The bearing stress is calculated by dividing the numerical reaction force by \( D \cdot t \), as per equation (1). The following observations can be made:

(i) Figure 9(a) shows that UBS increases with increasing friction coefficient between the pin and laminate, \( \mu_{\text{pin-laminate}} \), for the case where no contact between the plies is defined. The lowest value, \( \mu_{\text{pin-laminate}} = 0.05 \), results in under-prediction of the UBS, while the highest value, \( \mu_{\text{pin-laminate}} = 0.2 \), leads to over-prediction of UBS. The MCS is under-predicted for all the friction values.

(ii) Figure 9(b) shows a longitudinal section of the model without interply contact defined. For visualisation purposes, only eight plies are shown. The damage contours of the cohesive layers are indicated, with the label “\( d_{\text{interply}} \)” used in place of ABAQUS’s more cryptic “SDEG”. Distortion and penetration of ply elements into each other are seen, which is unrealistic.

(iii) Figure 9(c) shows the case where interply contact has been defined, and \( \mu_{\text{ply-plex}} \) is varied while keeping \( \mu_{\text{pin-laminate}} = 0.1 \). Note that \( \mu_{\text{ply-plex}} \) is only applicable once cohesive elements have deleted, and plies come into direct contact. The global response does not change much with \( \mu_{\text{ply-plex}} \), and MCS is still under-predicted. The computational time increases by 20% when \( \mu_{\text{ply-plex}} \) is increased from 0.4 to 0.8.

(iv) Figure 9(d) shows the longitudinal section when interply friction is defined. Penetration of the ply elements is avoided, and a reasonable damage morphology is obtained.

![Figure 9](image)

Figure 9: BK_D4_T2 specimen, (a) effect of \( \mu_{\text{pin-laminate}} \) (contact between ply elements not defined) on global response, (b) corresponding longitudinal section showing cohesive damage and ply inter-penetration, (c) effect of \( \mu_{\text{ply-plex}} \), (d) corresponding longitudinal section showing absence of ply inter-penetration.

From this first iteration, it was decided to fix the friction coefficients at \( \mu_{\text{pin-laminate}} = 0.1 \) and \( \mu_{\text{ply-plex}} = 0.4 \) for the remainder of the study. This value of \( \mu_{\text{pin-laminate}} \) was also used for single-lap joint modelling in [79].
The second iteration addresses the under-prediction of MCS. It was decided to look more closely at the selection of fracture toughness values ($G_{IC}$ and $G_{IIc}$) for the cohesive elements. Up to this point, all cohesive elements were assigned the material properties of a 0/0 interface, see Table 7. However, it is known from the literature for carbon fibre/epoxy systems [80, 81], that mode II fracture toughness for $+\theta/\theta$ and 0/0 interfaces, where $\theta$ is an arbitrary ply orientation other than 0°, are higher than for a 0/0 interface. The effect of interface ply orientations on mode I fracture toughness is relatively small. Table 8 summarises $G_{IC}$ and $G_{IIc}$ values found in the literature for IM7/8552, for the various interfaces in our models. As can be seen the $G_{IIc}$ values for the non-0/0 interfaces are higher than for the 0/0 interface. Using the 0/0 value for all interfaces in the model (as done so far) is likely to lead to early deletion of cohesive elements, when they should remain in the model, thus reducing the bending stiffness of the laminate and the resistance to pin movement. Thus, the FE model was modified to include the appropriate toughness values for each interface. Figure 10 shows the dramatic improvement this made in the prediction of the MCS. Meanwhile the already good prediction of UBS is virtually unaffected.

Table 8: Interface fracture toughness values of IM7/8552 for various interfaces [73, 74, 80, 82]

| Properties | Interfaces       | 0/0 | 0/45 | 0/90 | 45/-45 |
|------------|------------------|-----|------|------|--------|
| $G_{IC}$ (kJ/m$^2$) | 0.22 | 0.22 | 0.22 | 0.22 |
| $G_{IIc}$ (kJ/m$^2$) | 0.63 | 0.94 | 0.73 | 1.3   |

![Figure 10: Effect of interface fracture toughness properties for BK_D4_T2 specimen](image)

The third iteration deals with reducing the computational cost of modelling blocked laminates, i.e. laminates with $[45_m/-45_m/90_m/0_m]_l$ stacking sequence, with $m = 2, 3$. In the experiments, [17], it was found that most delaminations occurred between plies of different orientation, with very few existing within blocks of similarly-oriented plies. So to save computational cost, it is reasonable to place cohesive elements only between plies of different orientation, which reduces the number of delamination sites in 2 mm thick laminates from 15 to seven, and in 3 mm thick laminates from 23 to seven. This approach has also been used recently in [83], and it reduced the CPU time for our models from 98 hours to 43 hours for the 3 mm thick specimens. Figure 11 shows the global response for the model with a reduced number of interfaces compared to the model where all the possible interfaces are defined. Three configurations are shown, BK_D4_T2, BK_D8_T2 and BK_D12_T2. It can be seen that the prediction of UBS is scarcely affected, while the MCS is affected somewhat. This agrees with experimental evidence in [17], where, for blocked laminates, the initial delaminations at bearing failure (peak
load) were always between blocks of similarly-oriented plies, whereas later in the crushing process, some delaminations appeared within such ply blocks. Overall, the difference in MCS values due to use a reduced number of cohesive layers is relatively small, so this approach was used for the remainder of the paper.

In summary, the model calibration consisted of choosing appropriate values for friction coefficients, correct \( G_{\text{IIc}} \) values at each interface, and a simplification to reduce computation time for blocked laminates. No other calibration or tuning was applied in the remainder of this study.

![Graph showing experimental versus numerical pin movement at 30 mm pin displacement](image)

**Figure 11:** Global response for the case when cohesive layers are inserted only at different ply orientation interface, (a) BK_D4_T2, (b) BK_D8_T2, (c) BK_D12_T2

### 4.2 Prediction of global response, including the effects of varying joint parameters

In this section, the model is tested for its ability to predict the global response, through comparison with experimental results in [17]. In Figure 12(a), the movement of the pin, after 30 mm displacement, is shown for the model and the experiment for the BK_D4_T2 configuration. Because the tension rods in the experiment (shaded gold in Figure 1(b)) are only pinned at one end, they are free to rotate. Consequently the pin is free to follow the path of least resistance, and it sometimes veers away from straight-line motion, as shown in the example in Figure 12(a). The pin is also not restricted from sideways motion in the model, and it can be seen that the pin also veers away from straight-line motion. The exact direction of travel is unpredictable, as it depends very sensitively on the sequence of damage events in the laminate. One thing the model does not fully capture is the peeling off of surface 45° plies which can be seen in the experiment in Figure 12(a) and is discussed in [17]. However, this peeling process has a relatively minor effect on energy absorption. In Figure 12(b), the damage from a 4 mm pin is compared to that of a 12 mm pin. It is noticeable that the composite material on either side, and in front of, the pin, splay out-of-plane to a greater extent for the 4 mm pin than for the 12 mm pin. This will be discussed further below.

![Image showing experimental versus numerical pin movement](image)

**Figure 12:** (a) Experimental versus numerical pin movement at 30 mm pin displacement, (b) global “extended bearing failure” of the model for 4 mm and 12 mm pins.

To assess the capability of the model to predict the effects of varying pin diameter, laminate thickness and stacking sequence, all test configurations in Table 1 were simulated. Table 9 shows the predicted values of UBS,
MCS and SEA along with the experimental mean values. Shown also is the standard deviation in the experiments and the rank (1 to 15) of each configuration in terms of UBS, MCS and SEA.

The experimental UBS values vary over a wide range, from 298 MPa for the D12_T1 case (highest D/t value), up to 621 MPa for the DS_T4_T3 case (lowest D/t value and dispersed stacking sequence). The minimum and maximum UBS values predicted by the model are for the same cases, being 320 MPa for the D12_T1 case and 609 MPa for the DS_T4_T3 case. Eleven of 15 configurations show a difference of 5% or less between the model and the experimental mean, with the remaining four showing differences of 7%, 8%, 11% and 12%. The experimental and numerical rankings for UBS differ by no more than two for any configuration. Bearing in mind that the experimental mean values are from only four experiments (so could change if more tests were performed), and that the standard deviation of the experiments was up to 10.4%, the prediction of UBS by the model over a wide variation of configurations and UBS values, is excellent.

The predictions for MCS and SEA are not as good, which is not surprising, given the extremely complex damage and failure events that occur during crushing over such a long distance. Nonetheless, eight and 10 configurations are predicted within 10% of the experimental mean, for MCS and SEA respectively, with the remainder being within 20%. The experimental standard deviation for MCS and SEA is seen to be up to 14.8%. The experimental and numerical rankings for SEA again differ by no more than two for any configuration. The good prediction of UBS and SEA for blocked laminates suggests the successful representation of the in-situ effect for thick plies (Table 5).

Figure 13 shows a selection of experimental and numerical stress-displacement curves. The experimental curves are just one sample from among four repeat tests, so one should not expect exact agreement with the simulation. Figure 13(a) shows that the model correctly predicts that increasing pin diameter leads to decreased MCS/SEA. Similarly, Figure 13(b) shows that the model correctly predicts that increased thickness leads to increased MCS/SEA, and Figure 13(c) shows that, for 12 mm diameter pins, the model forecasts that a dispersed stacking sequence results in a higher MCS/SEA than a blocked stacking sequence, in accordance with the experiments. The reasons why pin diameter, laminate thickness and stacking sequence have these effects on the performance parameters are discussed in detail in [17].

One of the major findings in [17] was the strong correlation between UBS, MCS and SEA and the ratio of pin diameter to laminate thickness, $D/t$. In Figure 14, the predictions for the relationships between UBS, MCS and SEA and $D/t$ ratio are plotted together with the experimental results, for dispersed stacking sequences (on the left) and blocked stacking sequences (on the right). Results for 1 mm thick laminates are included in the dispersed stacking sequence plots. The predictions of UBS, MCS and SEA are seen to be excellent for dispersed stacking sequences, while the prediction of UBS versus $D/t$ is very good for blocked stacking sequences. The trend line equation for MCS (and consequently SEA) versus $D/t$ is somewhat off for blocked laminates, but the overall direction of the trend is more or less correct. Possibly, dispensing with the simplification in iteration 3 above, i.e. placing cohesive elements at all ply interfaces for blocked laminates, might give better results. Overall though, the model is shown to be capable of predicting the global response and performance parameters, UBS, MCS and SEA over a wide variation of geometric and material parameters.
Table 9 UBS, MCS and SEA mean experimental values versus model predictions, with percentage that the model value is above or below the experimental mean. Also shown are the relative standard deviation, RTSD, (standard deviation as a percentage of mean) in the experiments, and the rankings (1 – 15) of the configurations, found experimentally and numerically.

| Configuration | UBS Exp/FE (%) diff | UBS Exp (MPa) | UBS RSTD (%) | UBS Rank | MCS Exp/FE (%) diff | MCS Exp (MPa) | MCS RSTD (%) | MCS Rank | SEA Exp/FE (%) diff | SEA Exp (kJ/kg) | SEA RSTD (%) | SEA Rank |
|---------------|---------------------|---------------|--------------|----------|---------------------|---------------|--------------|----------|---------------------|----------------|--------------|---------|
| D4_T1         | 347 / 362 (+4%)     | ±4.6%         | 14 / 12      | 8 / 10   | 244 / 219 (-10%)   | ±1.6%         | 8 / 10        | 8 / 10   | 127 / 115 (-10%)   | ±7.6%         | 8 / 10       |
| D8_T1         | 355 / 365 (+3%)     | ±2.5%         | 13 / 11      | 14 / 14  | 184 / 153 (-17%)   | ±5.9%         | 15 / 15      | 15 / 15  | 84 / 84 (0%)       | ±11.3%        | 14 / 14      |
| D12_T1        | 298 / 320 (+8%)     | ±0.7%         | 15 / 15      | 15 / 15  | 136 / 144 (+6%)    | ±0.2%         | 15 / 15      | 15 / 15  | 66 / 76 (+16%)     | ±13.8%        | 15 / 15      |
| DS_D4_T2      | 512 / 509 (-1%)     | ±10.4%        | 3 / 3        | 24 / 4   | 298 / 278 (-7%)    | ±14.8%        | 4 / 4        | 4 / 4    | 164 / 142 (-13%)   | ±2.5%         | 4 / 4        |
| DS_D8_T2      | 489 / 436 (-11%)    | ±4.3%         | 5 / 7        | 258 / 75 | 258 / 273 (+6%)    | ±3.5%         | 7 / 5        | 7 / 5    | 128 / 141 (+10%)   | ±3.9%         | 7 / 5        |
| DS_D12_T2     | 449 / 455 (+1%)     | ±9.1%         | 8 / 6        | 211 / 92 | 211 / 146 (-12%)   | ±5.2%         | 11 / 12      | 11 / 12  | 103 / 93 (-10%)    | ±2.3%         | 11 / 13      |
| DS_D4_T3      | 621 / 609 (-2%)     | ±1.0%         | 1 / 1        | 345 / 305(+) | ±1.8%    | 3 / 1        | 178 / 189 (+) | ±4.0%   | 2 / 1        |
| DS_D8_T3      | 572 / 570 (-0.1%)   | ±3.3%         | 2 / 2        | 272 / 241(-11%) | ±6.6%    | 5 / 7        | 138 / 136 (-3%) | ±7.1%   | 5 / 6        |
| DS_D12_T3     | 498 / 503 (+1%)     | ±4.2%         | 4 / 4        | 232 / 228(-2%) | ±7.3%    | 9 / 8        | 116 / 124 (+7%) | ±5.6%   | 9 / 8        |
| BK_D4_T2      | 469 / 413 (-12%)    | ±4.1%         | 6 / 8        | 352 / 289 (-18%) | ±5.4%    | 2 / 3        | 175 / 146 (-16%) | ±8.3%   | 3 / 3        |
| BK_D8_T2      | 388 / 378 (-3%)     | ±5.7%         | 9 / 10       | 232 / 222 (-4%) | ±0.4%    | 9 / 9        | 116 / 116 (0%) | ±1%    | 9 / 9        |
| BK_D12_T2     | 384 / 352 (-3%)     | ±5.8%         | 11 / 11      | 176 / 197 (+12%) | ±11.4%   | 14 / 11      | 85 / 102 (+13%) | ±19%   | 13 / 11      |
| BK_D4_T3      | 460 / 481 (+5%)     | ±3.3%         | 7 / 5        | 369 / 294 (-20%) | ±0.3%    | 1 / 2        | 185 / 147 (+20%) | ±3.5%   | 1 / 2        |
| BK_D8_T3      | 381 / 407 (+7%)     | ±3.7%         | 10 / 9       | 269 / 255 (-5%) | ±1.9%    | 6 / 6        | 130 / 129 (-1%) | ±2.4%   | 6 / 7        |
| BK_D12_T3     | 359 / 363 (+1%)     | ±3.0%         | 12 / 12      | 205 / 183 (-11%) | ±11.7%   | 12 / 13      | 98 / 96 (+2%)  | ±8.1%   | 12 / 12      |
Figure 13: Numerical results versus experimental results, (a) 1 mm thick laminates tested with 4 and 12 mm diameter pins, (b) 1 and 3 mm thick laminates tested with 8 mm pin, and (c) 3 mm thick dispersed and blocked laminates tested with 12 mm pin.

4.3 Validation of mesoscopic response

In [17], tests were performed in which the pin displacement was halted at 0.75 mm (about 0.3 – 0.4 mm beyond peak load). The specimens were then scanned using three-dimensional computed tomography (3D CT), details of which can be found in [17]. In Figure 15, scans of the bearing plane are shown for 2 mm thick laminates with dispersed (top row) and blocked (bottom row) stacking sequences, with 4 mm (on the left) and 8 mm (on the right) diameter pins. Model results showing the interply damage variable, $d_{interply}$, at the same pin displacement, are shown alongside. Black dots indicate the approximate position where $d_{interply}$ first rises above 0 (although the different shades of blue are a little difficult to distinguish, so the positions are not exact).

As noted in [17], in the experiments, Figure 15(a), (c), (e) and (g), the outer plies (red labels) delaminate and splay outwards, while the centre plies (black labels) stay more or less aligned with the load and undergo crushing. Blocks of apparently undelaminated central plies at a distance of one laminate thickness (2 mm) from the hole edge are indicated. The size of these blocks and their degree of alignment with the loading direction were found to be good indicators of SEA in [17]. The alignment of the central blocks with the loading direction depends on the lateral support provided by the outer plies. Deeper delaminations tend to weaken this support.

In the simulations, Figure 15(b), (d), (f) and (h), the overall shape of the predicted deformation is correct. Outer plies delaminate and splay outwards, while the centre plies stay more or less aligned with the load. Furthermore, for the 4 mm pin, Figure 15(a), (b), (e) and (f), the outer plies splay out-of-plane to a greater extent than for the 8 mm diameter pin, Figure 15(c), (d), (g) and (h), in both the CT scans and the model images. This
was noted earlier for Figure 12. In addition, the delaminations extend further into the laminate (see black dots for model images) for the 8 mm pin, Figure 15(c), (d), (g) and (h), than for the 4 mm pin, Figure 15(a), (b), (e) and (f), in both the experiments and the simulations. These differences are due to the difference in width (4 mm versus 8 mm) of the crush zone in front of the pin. This zone tears away from the rest of the laminate, and the outer plies delaminate and buckle outwards. The support against ply buckling is less for the wider strip (larger pin), so the delaminations extend further into the laminate. As a consequence of these deeper delaminations, the support provided by the outer plies to the central plies is less for the larger pin, which allows the central plies to bend more easily out of the way of the oncoming pin. As discussed in [17], this is one of the key reasons why the performance parameters drop off with increasing pin diameter, and the model captures this effect well.

Figure 14: Numerical versus experimental variation of ultimate bearing strength (UBS), mean crushing stress (MCS) and mass-specific energy absorption (SEA) with D/t ratio, for dispersed (figures on left) and blocked (figures on right) stacking sequences.
Figure 15: Longitudinal sections of interrupted tests, versus simulations. Blocks of undelaminated plies at one laminate thickness from hole edge indicated on experimental images. Top row is dispersed stacking sequence, bottom row is blocked. Pin diameter is 4 mm in the images on the left, and 8 mm in the images on the right. Colours in model images indicate level of cohesive element damage. Black dots in numerical images give approximate point where cohesive layer damage begins.

Figure 15: Longitudinal sections of interrupted tests, versus simulations. Blocks of undelaminated plies at one laminate thickness from hole edge indicated on experimental images. Top row is dispersed stacking sequence, bottom row is blocked. Pin diameter is 4 mm in the images on the left, and 8 mm in the images on the right. Colours in model images indicate level of cohesive element damage. Black dots in numerical images give approximate point where cohesive layer damage begins.
Another observation is that the maximum delamination depth is greater for the blocked laminates, Figure 15(e), (f), (g) and (h), than for the dispersed laminates, Figure 15(a), (b), (c) and (d), which is a contributing factor towards the lower performance of the blocked laminates. Again this predicted by the simulations.

In Figure 16, the stress state in the cohesive elements in front of the pin is examined in detail for the DS_D4_T2 case. Figure 16(a) shows the location at which cohesive zone tractions were extracted (first cohesive element in front of the pin in each layer) and the coordinate system used. The normal traction, $\sigma_{33}$, relates to opening Mode I when positive, while $\tau_{13}$ relates to sliding Mode II, and $\tau_{23}$ relates to tearing Mode III. Figure 16(b) shows $\sigma_{33}$ (top-left), $\tau_{13}$ (top-right), $\tau_{23}$ (bottom left), and the normal traction for each ply at peak load (black squares) and at a 10% drop in load post-peak (red triangles). The quantity plotted in the bottom-right figure is the left-hand side of equation (31), which indicates delamination initiation when it reaches one. Note that $\sigma_i = \sigma_{33}$, $\sigma_{II} = \tau_{13}$ and $\sigma_{III} = \tau_{23}$. The Mode II/III strength, $\sigma_{II}^n = \sigma_{III}^n = 90$ MPa (see Table 7) is shown as a vertical dashed line on the shear traction plots.

At peak load, $\sigma_{33}$ is negative for all plies. Thus the normal tractions do not cause delamination. $\tau_{13}$ exceeds $\sigma_{II}^o$ at interfaces 1 and 15 (see numbering in Figure 16(a)), which are $45^\circ/-45^\circ$ interfaces. Meanwhile, $\tau_{23}$ does not exceed $\sigma_{III}^o$ for any ply. When the tractions are combined, we see that $\left( \frac{\sigma_i}{\sigma_i^o} \right)^2 + \left( \frac{\sigma_{II}}{\sigma_{II}^o} \right)^2 + \left( \frac{\sigma_{III}}{\sigma_{III}^o} \right)^2$ far exceeds one at interfaces 1 and 15 indicating delamination initiation. Additionally though, it also slightly exceeds one at interfaces 5 and 11. Sliding Mode II is the biggest contributor at both sets of interfaces, followed by tearing Mode III. Opening Mode I plays no part.

After a 10% drop in load (post-peak), the tractions have changed significantly. $\sigma_{33}$ is still negative for all plies so still plays no part in delamination. $\tau_{13}$ is now largest at interfaces 3 and 13, indicating Mode II-driven delamination at these $0^\circ/90^\circ$ interfaces, while $\tau_{23}$ is highest at interfaces 5 and 11. The bottom-right figure shows that most of the outer plies have delaminated (note that in some cases, the delamination initiation criterion value has fallen back to less than it was at peak load, but this does not indicate the absence of delamination, since delamination is non-recoverable). A centre block of five plies ($90/0/90/0\pm45$) is undelaminated, which correlates quite well with the CT scan in Figure 15(a).

In Figure 16(c), compressive fibre damage is shown at peak load, and at 10% drop in peak load (post-peak). It can be seen that at peak load, fibre compressive damage is already well under way, particularly in the $0^\circ$ plies, but also to a lesser extent in the $\pm45^\circ$ plies. However, none of the elements are shaded red, indicating that no elements have completely failed due to fibre compressive damage yet. By the time the load drops by 10%, multiple elements are indicating complete fibre damage failure. The first drop in load from its peak value thus appears to be triggered by delamination (not the initiation of fibre damage), which most likely then accelerates the accumulation of fibre damage.
(a) Location and definition of cohesive zone tractions for plots

(b) DS_D4_T2 tractions and delamination initiation criterion

(c) DS_D4_T2 fibre compressive damage at peak load and at 10% load drop (post-peak)

Figure 16: (a) Location of cohesive zone elements for which tractions are plotted, and definition of tractions sign convention, (b) Traction plots for DS_D4_T2 case, at peak load and at 10% load drop (after peak load), (c) fibre compressive damage at peak load and at 10% load drop.
Figure 17 shows a similar set of plots for a blocked stacking sequence, BK_D4_T2. Once again, the normal tractions are negative for all plies at both load levels, so delamination is entirely shear-driven. This time, the tractions do not change nearly as dramatically between peak load and post-peak (10% load drop), as they did for the dispersed stacking sequence. Delamination occurs at interfaces 2 and 14 (45°/-45° interfaces) and interfaces 4 and 12 (-45°/90° interfaces). No cohesive elements exist between plies of the same orientation, so no delamination is possible at odd-numbered interfaces. A central block of eight undelaminated plies (90°/0°/90°/0°) is predicted after 10% load drop (bottom right plot), which correlates exactly with the CT scan in Figure 15(e).

Once again fibre compressive damage has initiated before the peak load, but no elements have fully failed due to fibre damage at peak load. So the initial drop in load is delamination driven.

(a) BK_D4_T2 tractions and delamination initiation criterion

(b) BK_D4_T2 fibre compressive damage at peak load and at 10% load drop (post-peak)
In Figure 18(a) and (b) scans of the bearing plane are shown for 3 mm thick laminates, tested with a 12 mm diameter pin until 0.75 mm displacement, for dispersed (DS_D12_T3) and blocked (BK_DS_T3) stacking sequences respectively. The dispersed laminate contains a large number of delaminations, extending up to 6 mm into the laminate, and leaving a central block of about nine undelaminated plies at one laminate thickness from the hole edge. The blocked laminate separates at the interfaces between blocks of three similarly oriented plies, resulting in fewer but deeper delaminations (up to 9 mm), and central block of only six undelaminated plies. The blocked laminate has significantly lower UBS and SEA than the dispersed laminate (see Table 9) due to the deeper delaminations and the smaller block of undelaminated plies. Below the CT scans are plots of fibre compression damage at 0.25 mm, 0.5 mm and 0.75 mm pin displacement, and also a 3D view. Fibre compression damage begins in the 0° plies, but spreads to all plies eventually. The predicted deformation agrees well with the experiments, and in the 3D views, it can be seen that fibre damage in the central plies spreads more widely to either side of the hole for the blocked laminate than for the dispersed laminate.

In Figure 19, a view of the pin-laminate contact region shows the progression of fibre compression damage (on the left) and fibre matrix damage (on the right) for the DS_D8_T2 configuration, with a comparison with a CT scan. It can be observed that at 0.75 mm pin displacement, the FE model shows good agreement with the CT scan in terms of the spread of damage and level of brooming of the outer plies.

5. Conclusions

A 3D finite element approach, incorporating a mesoscale damage model, has been developed for the prediction of extended bearing failure, which occurs in tension-absorber joints. The model is compared with experimental data covering a wide range of parameter variations, and very good agreement is found at both the global and mesoscopic scale. Using material parameters for IM7/8552 readily available in the literature, the model is capable, without any special tuning, of predicting the effects of variable pin diameter, laminate thickness and stacking sequence on the key performance parameters: ultimate bearing strength, mean crushing force and specific energy absorption. The model was able to rank 15 different joint configurations in terms of performance with a high degree of accuracy. Predictions of bearing strength were mostly within 5%, and all within 12%, of the experimental mean. Predictions of specific energy absorption were mostly within 10%, and all within 20%, of the experimental mean. The meso-scale response also closely followed the experiments, as shown by comparison with CT scans. The general shape of the ply deformations, depth of delaminations, size of the central block of undelaminated plies, and evolution of fibre and matrix damage, matched the experiments well. Overall, the model is found to be genuinely predictive for an extremely challenging problem. Although the model is computationally expensive, the approach will become more practical as computer hardware continues to develop.

In future work, the model will be extended to include strain-rate dependent material properties and tested on dynamic pin-bearing tests which are currently under review for publication. In addition, another test series involving non-quasi-isotropic layups has also been performed, and the model will be tested on those layups. A numerical optimisation study will then be performed to find the highest performing stacking sequences for tension-absorber joints.
45° 
-45° 
90° 
0° 
90° orientation change
Figure 18: Evolution of fibre compressive damage with increasing pin displacement, i.e., 0.25 mm, 0.5 mm and 0.75 mm, and comparison with CT scans of interrupted tests at 0.75 mm displacement for DS_D12_T3 specimen (figures on left) and BK_D12_T3 specimen (figures on right)
Figure 19: (a) CT scan section of pin-hole contact region at 0.75 mm displacement for DS_D8_T2 specimen, (b) predicted intralaminar damage at the pin-hole contact region at 0.25 mm, 0.5 mm and 0.75 mm pin displacement.

Acknowledgements

This work was supported by EU Horizon 2020 Marie Skłodowska-Curie Actions Innovative Training Network- ICONIC [grant agreement number: 721256]. The authors also wish to acknowledge the Irish Centre of High-End Computing (ICHEC) for the provision of computational facilities.

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