Tensile failure of laminates containing an embedded wrinkle; numerical and experimental study

Supratik Mukhopadhyay*, Mike I. Jones, Stephen R. Hallett

University of Bristol, Advanced Composites Centre for Innovation and Science, Queens Building, Bristol BS8 1TR, United Kingdom

*Corresponding author. Email: s.mukhopadhyay@bristol.ac.uk

Abstract

The failure of a quasi-isotropic composite laminate containing an embedded out-of-plane fibre wrinkle defect was investigated under tension loading. Laboratory test specimens with controlled severity of fibre waviness were manufactured. Along with recording load-displacement data, high resolution camera images were taken at regular intervals which monitored the initiation and interaction of different damage mechanisms during test. Three-dimensional FE models were built following the geometry of actual test specimens. The information obtained from the tests was used to develop user material subroutines, implemented in Abaqus/Explicit as continuum damage and cohesive zone models for intra- and inter-ply failure respectively. The results of the simulations showed very good correlation with test observations in terms of failure load, location of damage initiation and interaction between different damage mechanisms for a range of waviness cases tested.

Keywords: A. Carbon fibre, B. Delamination, Fracture C. Finite element analysis (FEA)
1 Introduction

Composite materials are increasingly being used to manufacture structural parts in the aerospace and automotive sectors for the advantages they provide in terms of weight saving, ability to tailor stiffness in safety-critical regions, good energy absorption capacity etc. One key challenge that limits their use is the presence of various process-induced defects [1]. These defects typically act as sites of potential failure initiation in composite laminates and reduce their load bearing capacity. Out-of-plane fibre waviness or ‘wrinkling’ is one such defect which typically is found in thick section composite structures. Wrinkles are identified by the fibre-misalignment through-the-thickness of the laminate resulting in localized wavy regions (Figure 1(a)). Following [2], the geometry of a typical wrinkle is described by three parameters $\delta$, $\lambda$ and $\theta$ which are respectively the amplitude, wavelength and waviness angle of the defect (Figure 1(b)). The reasons behind wrinkle formation is still an active area of research as the mechanisms are many and varied. For example, the wrapping of pre-preg tapes around drums for storage causes a difference in path-length between the outside and inside surface of the pre-preg, promoting fibre wrinkling [1], or Automated Fibre Placement (AFP) laying heads turning on small radii will cause fibres to buckle out-of-plane [3]. Recently Lightfoot et al. [4] suggested a new mechanism of wrinkle generation in multidirectional laminates laid on curved radii, due to generation of relative shear forces between the stiff and compliant ply orientations during consolidation.

Tensile failure of composite laminates containing wrinkle has been previously studied by El-Hajjar et al., Bloom et al. and Makeev et al. [5–7]. Steel rods of different diameter were used by El-Hajjar et al.[5] to artificially induce waviness in unidirectional and multidirectional carbon-epoxy tests. In multidirectional laminates, they noted delamination and internal ply failures caused a small drop in load-displacement response much earlier than the final failure by tensile fibre fracture. Intermittent images were taken to observe damage evolution at the
gauge section. Analytically, the axial stiffness of the specimens was predicted using lamination theory and by approximating the waviness using a Gaussian function. Bloom et al. [6] used the technique of fitting slightly oversized plies to a mould to generate artificial wrinkling in unidirectional and woven glass-epoxy composites. The strain distribution on the surface of the specimens near the wrinkle was measured using Digital Image Correlation (DIC) and video gauge techniques. In both composite systems, the final failure was found to be fibre fracture due to high tensile strain concentration in the wrinkle, which also caused subsequent delamination between plies. Simple design guidelines, which related the failure stress to the volume of wrinkled plies in the laminate, gave a significant over prediction of the knockdown levels. They concluded that a detailed 3D finite element analysis might be necessary for this problem due to the complex failure mechanisms and their interaction. Makeev et al.[7] tested thick-section carbon-epoxy multidirectional tape laminates with different grades of wrinkle severity. DIC was used to measure strain in components during loading until complete failure. Plane stress Finite Element (FE) models were built with nonlinearity in the interlaminar shear stress-strain relationship incorporated in the model. Failure initiation was predicted using maximum interlaminar tensile stress criteria and Hashin mixed-mode criteria. They found that including the nonlinear shear behaviour in the model was essential to get correct prediction of failure load. They were also able to predict delamination initiation in the location of the wrinkle, which was experimentally confirmed. The flexural strength of carbon-epoxy unidirectional laminates containing a wrinkle was studied by Potter et al.[8]. Under four point bend loading a strength reduction of as much as 70% of pristine value was reported for samples appearing to fail on the tensile surface.

The work presented in this paper builds on previous work to be found in the literature and offers an improved predictive capability for failure of wrinkled laminates under tension, by particularly taking into account both intra and inter-laminar damage modes and their
interaction in a 3D finite element framework. The organization is as follows: In Section 2 the experimental methods for manufacturing and testing wrinkle specimens are described. In Section 3 the FE modelling strategy is introduced, which also outlines the formulations of different damage models used. In Section 4, the results of the test and model are presented along with a detailed comparison between the two. Finally in Section 5, conclusions are drawn based on the above study.

2 Experimental procedures

2.1 Wrinkle specimen manufacture

Commercially available (Hexcel Composites Ltd) IM7/8552 pre-preg tapes with 0.125 mm nominal cured ply thickness were laid up by hand to make a quasi-isotropic ([+45/90/-
45/0]s) laminate. The test specimen dimensions were 250mm×30mm×6mm with a gauge length of 150mm after tabbing. Wrinkle-free baseline specimens with the same dimensions were also manufactured for comparison of failure load. Out-of-plane fibre waviness was created in the central region by inserting thin 90° ply strips adjacent to the full-length 90° plies in certain locations through the thickness of the laminate (Figure 2(a) & (b)). Although during the natural process of wrinkle defect formation, usually neat resin fills the gap around the wavy fibres, it was difficult to recreate the same effect when inducing a wrinkle artificially. In place of neat resin, the use of 90° strip plies can be justified by the fact that, these plies being completely orthogonal to the loading direction, their behaviour was resin-dominated and minimally affected the overall load-displacement response. Also, this method of producing the wrinkle avoided the introduction of any pre-cured or foreign substance in the lay-up or new interfaces, thus minimising the chance of delamination along weak interfaces. Eight test specimens for each of the three different wrinkle severities were produced by varying the thickness and position of 90° strips in the lay-up. Those were named
level#1, level#2 and level#3 where a higher number indicates a higher wrinkle angle. The determination of the wrinkle angles was done by taking images of the edge of the test specimen and measuring the angle that a tangent drawn at a wavy ply made with a straight reference ply (Figure 3). The average wrinkle angles for the three levels were approximately 4.9°, 8.8° and 12.1° respectively. In regards to this, it should be mentioned that the wrinkle aspect ratio (waviness height/waviness amplitude) is also commonly used as an index to estimate wrinkle severity. The authors noticed when analysing microscope images of wrinkle profile that even for the same wavelength and same amplitude, there can be local variations in the wrinkle angle on different parts of the wave which is due to the fact that geometrically, the wrinkle profile matches more a trigonometric waveform rather than an inclined straight line. It is this local maximum slope of the waviness which is crucial for failure initiation. Hence the local maximum wrinkle angle of the wave was used as a suitable parameter to estimate the wrinkle severity.

2.2 Tensile testing

Uniaxial tensile loading until failure was performed on the specimens using a servo-hydraulic tensile testing machine of 250kN capacity. The specimens were loaded in displacement control with a loading rate of 0.5 mm/min. Load versus crosshead-displacement data was recorded during the tests. In addition, a high resolution camera was focussed on the gauge section of the specimen and took images at regular intervals during test to monitor the sequence of events. The load-displacement data and the camera images were later post-processed to uniquely identify the initiation and interaction of damage mechanisms before final failure. This information also helped in validation of the FE analysis, which is described in Section 3.
3 Finite element modelling

3.1 FE mesh of wrinkle specimen

The geometry and dimensions of the finite element models were taken directly from images of wrinkle specimens, obtained from optical microscopy, with the gauge length in the model reduced to 90mm for computational efficiency (Figure 4). A “MATLAB” based pre-processor created the wrinkled mesh of the laminate, which was approximated by a cosine function. From a uniform mesh this modified the through-thickness direction co-ordinates of nodes and created the waviness as given below:

\[ h_w = h_0 + \Delta h \]  \hspace{1cm} (1)

where \( h_w \) is the through-thickness nodal coordinate in the wrinkled configuration and \( h_0 \) is the corresponding value in a wrinkle-free flat laminate. The parameter \( \Delta h \) follows a sinusoidal waviness as follows:

\[ \Delta h = \begin{cases} \frac{B\delta}{2} \cos\left(\frac{2\pi x}{\lambda}\right) & \text{if } -\frac{\lambda}{2} \leq x \leq \frac{\lambda}{2} \\ 0 & \text{otherwise} \end{cases} \]  \hspace{1cm} (2)

where \( x \) is the dimension along the length of the laminate, \( \lambda \) is the wavelength of the wrinkle, \( \delta \) is the amplitude of the wave and \( B \) is the variation of amplitude through-the-thickness. The value of \( B \) is unity on the centreline and reduces linearly through the thickness to zero at the surface. Using such a cosine approximation for the waviness, the wrinkle angle, \( \theta \), can be analytically derived [2] as:

\[ \theta = \tan^{-1}\left(\frac{\pi \delta}{\lambda}\right) \]  \hspace{1cm} (3)

The wrinkle angle from Eq. (3) was compared with the results from the tangent based measurement technique on specimen images (described in Section 2) for the three levels of manufactured wrinkles. Figure 5 shows that the cosine approximation of waviness accurately
reproduces the expected wrinkle angle. Hexahedral 8-noded C3D8R elements were used to model the plies with one element through-the-thickness per ply. Zero thickness 8-noded cohesive COH3D8 elements were inserted between ply elements to model the interface. A fine mesh with in-plane dimension ~ 0.25mm × 0.25mm was maintained in the location of the wrinkle and towards the laminate edges (where delaminations initiated first), while a comparatively coarser mesh was used elsewhere for computational efficiency. A typical FE model of the tension specimen consisted of 270,072 C3D8R elements and 153,912 COH3D8 elements.

3.2 Damage modelling

3.2.1 Undamaged material response

The material model used in the analyses was implemented as an Abaqus/explicit VUMAT user material subroutine. Linear-elastic orthotropic thermo-mechanical properties were assigned to the plies except for the in-plane and through-thickness shear response which was non-linear. The linear-elastic material properties are given in Table 1. The data points for the non-linear shear for IM7/8552 were obtained from [7] with an exponential curve of the following form to fit the data:

\[ \sigma_{ij} = \text{sgn}(\gamma_{ij})(A(1-e^{-B\rho_{ij}})) \quad i = 2,3 \]  

(4)

where the index numbers follow the usual notation used for composite materials and \( A \) and \( B \) are two parameters to be obtained by a least-square fit to the experimental data. In this work, \( A \) and \( B \) were found to be 145 and 38 by fitting to the data given in [7]. These two parameters were supplied to the VUMAT subroutine as user inputs. The loading response followed this non-linear behaviour but the unloading response for these two shear components was considered purely linear, following the initial slope of the loading curve.
3.2.2 Transverse matrix cracking

Development of matrix cracks is common while loading multi-directional laminates in tension. The cracks themselves don’t significantly reduce the overall stiffness of the structure, but they are known to initiate and interact with other failure processes such as delamination. For this reason, a continuum damage model for matrix cracking was implemented in the material user-subroutine. This formulation followed directly from the work of Pinho et al. [9,10].

Depending upon the three-dimensional stress state, this criterion selects a fracture angle by maximizing an initiation law at every possible fracture plane in \([0, \pi]\). However, as reported by Catalanotti et al. [11], the quadratic law proposed in [9] has limitations in correctly predicting fracture angle in pure transverse tension. Therefore, a more accurate initiation law [11] is used in this work:

\[
f_{mt} = \left( \frac{\sigma_N}{S_T} \right)^2 + \left( \frac{\tau_L}{S_L} \right)^2 + \left( \frac{\tau_T}{S_T} \right)^2 + \lambda \left( \frac{\sigma_N}{S_T} \right) \left( \frac{\tau_L}{S_L} \right) + \kappa \left( \frac{\sigma_N}{S_T} \right)
\]

where \(f_{mt}\) is equal to unity at damage initiation. The normal traction on the fracture plane is \(\sigma_N\) while the two shear tractions are respectively \(\tau_T\) and \(\tau_L\) (see Figure 6). \(S_L\) and \(S_T\) are the matrix in-plane and transverse shear strengths respectively. \(S_T\) is obtained from transverse compressive strength \(Y_T\), following the procedure given in [9]. The parameters \(\lambda\) and \(\kappa\) are given by:

\[
\begin{align*}
\kappa &= \frac{(S_T)^2 - (Y_T^{\mu})^2}{S_T Y_T^{\mu}} \\
\lambda &= 2\mu_L \frac{S_T}{S_L} - \kappa
\end{align*}
\]

where \(Y_T^{\mu}\) is the “in-situ” transverse tensile strength and \(\mu_L\) is the longitudinal friction coefficient. It is well established that transverse tensile strength of a composite ply is
different from its nominal value (which is test data extracted from unidirectional laminate) when it is embedded in a multidirectional laminate and constrained by plies of other orientation. This effect is known as “in-situ effect”[12,13]. Because the present study involves matrix cracking in a multidirectional laminate, the “in-situ” tensile strength (given for the present materials system in [11] derived following the analytical expression proposed in [13]) is considered more appropriate to have accurate predictions.

Once damage was initiated, an equivalent stress $\sigma_{\text{mat}}$ was determined:

$$\sigma_{\text{mat}} = \sqrt{\sigma_n^2 + \tau_T^2 + \tau_L^2}$$  \hspace{1cm} (7)

Following this, an in-plane resultant shear strain $\gamma_{\text{mat}}$ and the damage-driving strain $\varepsilon_{\text{mat}}$ was deduced:

$$\gamma_{\text{mat}} = \left| \gamma_T \cos \beta + \gamma_{\ell, L} \sin \beta \right|$$  \hspace{1cm} (8)

$$\varepsilon_{\text{mat}} = \frac{\sigma_n}{\sigma_n} \varepsilon_n \sin \omega + \gamma_{\text{mat}} \cos \omega$$  \hspace{1cm} (9)

where $\varepsilon_n$ indicates normal strain and $\gamma_T$, $\gamma_{\ell, L}$ are the shear strains on the fracture plane which are obtained by projecting the global strains on the fracture plane. The Macaulay bracket on $\sigma_n$ indicates that negative normal traction component does not contribute to driving strain, as it tends to close any cracks. The superscript ‘el’ in Eq. (8) indicates only the elastic component of the in-plane shear strain participates in the fracture process. Also, $\beta$ and $\omega$ are angles between different traction components in the fracture plane (see [10] for details) which are required to orient the driving strain in the direction of effective stress.

A mixed-mode critical fracture energy $G_m$ was evaluated based on the power law [14]:
In Eq. (10), the superscript \( '0' \) indicates the value of the variable at damage initiation. The fracture toughness in the normal direction to the fracture plane is given by \( G_N \), while in the other two shear directions are given by \( G_T \) and \( G_L \) respectively. Assuming a bilinear relation between the equivalent stress and equivalent strain, \( \varepsilon_f \), the value of equivalent strain at complete failure was given by:

\[
\varepsilon_f = \frac{2G_m}{\sigma_0 l_c} \tag{11}
\]

The characteristic length \( l_c \), which ensured mesh size independent energy consumption [15], was calculated by projecting the length of the element edges on the fracture plane. Following this, a damage variable \( D_{mc} \in [0,1] \) was introduced:

\[
D_{mc} = \max \left\{ 0, \min \left\{ 1, \frac{\varepsilon_f (\varepsilon_{mat} - \varepsilon_0)}{\varepsilon_{mat} (\varepsilon_f - \varepsilon_0)} \right\} \right\} \tag{12}
\]

The traction components acting on the fracture plane were degraded using this damage variable:

\[
\begin{align*}
\sigma_N & \rightarrow (1 - D_{mc})\sigma_N \\
\tau_T & \rightarrow (1 - D_{mc})\tau_T \\
\tau_L & \rightarrow (1 - D_{mc})\tau_L
\end{align*} \tag{13}
\]

In the actual implementation, at complete failure, the damage parameter \( D_{mc} \) was held at 0.98, but not exactly unity to avoid numerical instabilities in the model. The material parameters used for this damage model are given in Table 2.

A preliminary study was done to confirm mesh-independency of the matrix crack model. For this, a simple geometry of size 5mm×5mm×0.125mm was meshed with 1, 4, 9 and 16
C3D8R elements and loaded under transverse tension. One element through-thickness was used in each case. In this test a low failure initiation stress 30 MPa was used which allowed for a coarse mesh and preventing snapback. Global load vs. displacement curves are shown in Figure 7 indicate mesh independent results. Another important observation was the correct prediction of the fracture angle under transverse tensile loading. The fracture angle was output as a state variable in the material subroutine. It was noticed that when a quadratic interaction criterion [9] was used, the predicted fracture angle was 33° instead of 0° (the same observation also made in [11]) and consequently resulted in slightly higher fracture energy. The improved initiation criterion (Eq. (5)) was able to correctly predict the fracture angle as 0° and solve this problem.

3.2.3 Delamination

A cohesive constitutive law, due to Jiang et al [16] was implemented as a user subroutine and used to model interfacial delamination. For completeness, a brief summary of the formulation is outlined here.

The cohesive law relates relative nodal displacements of the cohesive elements to corresponding traction components. Since a VUMAT subroutine only provides interfacial strain increments and not the nodal displacements, the later were extracted from the strain components following Gonzalez et al [17] as:

\[
\begin{align*}
\delta_1 &= h_e e_{33} \\
\delta_2 &= 2h_e e_{23} \\
\delta_3 &= 2h_e e_{13}
\end{align*}
\]  

(13)

where \( \delta_1, \delta_2 \) and \( \delta_3 \) are the mode I, mode II and mode III relative displacements between the surfaces of the cohesive element. \( h_e \) is the characteristic thickness of the cohesive element. \( e_{33}, e_{23} \) and \( e_{13} \) are the normal and two shear strains respectively. In the present implementation, the nodes at the top surface of the cohesive elements were collapsed to the
corresponding nodes at the bottom surface, to effectively have zero-thickness interfaces, while the characteristic thickness $h_e$ was set to unity so that the strains became equal to relative displacements.

A mixed-mode constitutive law for delamination can be established afterwards as a three-dimensional map with the pure mode I behaviour indicated in the $0 - \sigma - \delta_I$ plane, while mode II behaviour is indicated by $0 - \sigma - \delta_{II}$ plane (Figure 8). In this formulation, a combined mixed-mode displacement $\delta_m$ was defined as follows:

$$
\delta_m = \sqrt{(\delta_I)^2 + (\delta_2)^2 + (\delta_3)^2}
$$

where the Macaulay bracket on $\delta_I$ means compressive normal stresses do not initiate delamination as they tend to close any cracks. Before damage initiation, the top and bottom surface of cohesive element was assumed to be connected by springs of high stiffness $K_I$ and $K_{II}$ in mode I and mode II directions respectively that relate interfacial tractions with corresponding displacements. A quadratic interaction criterion was used to initiate damage:

$$
\sigma_{III}^2 + \sigma_{II}^2 = \sigma_{I max}^2
$$

where $f_{delam}$ is equal to unity at initiation. $\sigma_{I max}$ is the interlaminar normal strength and $\sigma_{II max}$ is the interlaminar shear strength.

The propagation was controlled by the power law criterion [14]:

$$
\left( \frac{G_I}{G_{IC}} \right)^\alpha + \left( \frac{G_{II}}{G_{IIc}} \right)^\alpha = 1
$$
where $G_I$ and $G_{II}$ are the mode I and mode II energy release rates while the subscript ‘c’ indicates the fracture toughness in respective modes. A damage variable $D_{delam} \in [0,1]$ was used to linearly degrade the traction components to zero from their initiation value:

$$D_{delam} = \max \left\{ 0, \min \left\{ 1, \frac{\delta^f_m - \delta^e_m}{(\delta^f_m - \delta^c_m)} \right\} \right\}$$

The superscripts ‘$e$’ and ‘$f$’ on $\delta_m$ indicate values of $\delta_m$ at damage initiation and complete failure respectively. Finally, the maximum value of $D_{delam}$ over time was considered always to be the actual extent of interfacial delamination, to enforce the damage-irreversibility. The material parameters used for this damage model are summarized in Table 3.

It is already well established that this cohesive formulation remains mesh size insensitive with at least three elements in the cohesive zone [18]. In present work, this was ensured by using a fine mesh (see Section 3.1) in critical regions of the model.

### 3.2.4 Fibre tensile failure

A simple stress based criterion was used for fibre tensile failure in this work:

$$f_f = \frac{\sigma_{11}}{X_T}$$

where $f_f$ is the fibre tensile failure law which is unity at initiation. $\sigma_{11}$ is the fibre direction stress and $X_T$ is the fibre tensile strength which was taken as 2724 MPa for the IM7/8552 material, from [19]. For simplicity, no user material subroutine was used in the model for this failure mode. The contour plot of the fibre-direction stress $\sigma_{11}$ was probed during the analysis, and failure was considered if it exceeded the user specified fibre tensile strength $X_T$. 
4 Results

4.1 Test

4.1.1 Tensile strength reduction in wrinkle specimens

Six specimens were generally tested for each of the wrinkle severity level#1 to #3 to have statistically significant results. All the specimens failed in the gauge section in the location of the wrinkle as expected. Although the load-crosshead displacement curve of pristine and level#1 specimens showed no signs of damage until complete failure, the level#2 and level#3 specimens showed a small drop in load at certain load levels before final failure which was consistent among all the specimens tested (see load-drop between points ‘2’ and ‘3’ in a typical level#3 specimen in Figure 9(a)). The cross-sectional stress levels corresponding to the first load-drop and final failure load for all three wrinkle severities are summarised in Table 4.

4.1.2 Failure mode identification

As mentioned in Section 2.2, a high resolution video camera was used to capture images of the wrinkle section during test. Points 1-7 in Figure 9(b) show a sequence of images taken during loading a typical level#3 specimen until complete failure. Between point 2 and point 3 the development of transverse matrix cracks and delamination between 45°/90° interfaces can be seen, which causes the small early load drop. As the specimen was loaded further, more internal matrix cracks, surface splits and delamination ensue before complete failure by fibre tensile fracture of the 0° plies (points 4 to 7 in Figure 9(b)). Further to the test video imagery, two tests of level#3 specimens were interrupted after the first load drop and micro X-ray CT-scanned to determine the internal state of damage at that point. The CT scan data was post processed by identifying different grey scale values (related to the local X-ray absorption coefficient of the material) corresponding to different damage modes and colour coding them.
for the different delamination planes using the same technique as described in [20]. Figure 10 shows extensive delamination in the wrinkle region spanning the entire width of the specimen and matrix cracks both in 90° and 45° laminates.

4.2 Model

4.2.1 Failure prediction and comparison with tests

A two-step analysis was performed with the first step simulating the residual stress development inside the laminate due to cooling down from final cure temperature of 180°C to room temperature at 20°C. In the next step, the ambient room temperature was maintained and displacement based tensile loading was applied at the boundary until complete failure. The results are plotted in Figure 11 along with experimental values for comparison. Clearly, the models are able to successfully predict both the first load-drop (delamination and matrix crack development) and final failure stress levels due to tensile fibre fracture, as seen in tests.

4.2.2 Failure modes and failure locations

It was found from the models that damage always initiated at the wrinkle due to concentration of the through-thickness shear stresses. In a multidirectional laminate, as in the present case, this effect was multiplied by the in-plane shear stress localization near the laminate edge which caused delaminations to always initiate from the edges of the laminate. In this regard, it was observed that simple 2D models (generalized plane-strain boundary conditions applied on both edges of a single element wide model) significantly over predicted the load-level corresponding to delamination initiation by almost 50% because they lacked the effect of stress concentration at the laminate edges. This necessitated the use of 3D models which correctly captured the initiation and propagation of the different damage modes. It was also noted that both the matrix crack model and the delamination model were essential to correctly capture the global failure behaviour of the structure. For example, in the
level#3 specimen, in absence of both the delamination and matrix crack model, the final failure load was over-predicted by 52% while in absence of only the matrix crack model and assuming delamination alone to cause fibre failure, the over-prediction was 16%. This clearly shows that both damage models were crucial in this case, where they interacted among themselves to cause failure. In Figure 12, a comparison is drawn between the damage states in the model and test results for level #3 at a cross-sectional stress level of ~ 450 MPa which refers to position ‘5’ in the load-displacement curve in Figure 9(a). Numerals are used to identify and compare similar damage locations between test and the model. It can be seen that the model not only predicted the principal damage locations accurately, but also was able to successfully capture the interaction between matrix cracks and delaminations forming ‘Z’-cracks which transferred delamination from one interface to another as also observed in the tests. An overview image of the delamination prediction, colour coded to be similar to figure 10, is shown in figure 13.

4.2.3 Mesh sensitivity of results

The Mesh refinement studies were performed on the model of the level#3 specimen which was the most severe wrinkle configuration. Figure 13 shows cross-sectional stress vs displacement curve for three different mesh densities. The results presented in section 4.2.1 and 4.2.2 are from the model with 423,984 elements with a minimum in-plane element size of 0.25mm×0.25mm. A coarser mesh with smallest element size of 0.5mm×0.5mm and a moderately fine mesh with smallest element size of 0.3mm×0.3mm were also investigated. It can be seen that the results remained relatively mesh-size insensitive provided a reasonably fine mesh was used in the wrinkle region where the delaminations and matrix cracks initiated first. Also, as can be noted, the first load drop at ~400 MPa before final failure (which happened due to delamination, see Section 4.1.1) was correctly captured in all the cases. The mesh-orientation bias of the evolving cracks, which is a well-known problem of continuum
damage modelling, was taken care of by maintaining an in-plane aspect ratio of the elements close to unity near the wrinkle region and towards the laminate edges from where the cracks were seen to initiate. A biased mesh with relatively coarser elements was used in the interior of the laminate and away from the wrinkle (Figure 4). Although this approach couldn’t completely eliminate the problem, it was seen to reduce the mesh-orientation bias of the cracks noticeably.

4.2.4 Effect of stacking sequence

The above study has clearly validated the numerical model against the experimental results, thus showing the effectiveness of the analysis techniques proposed. In terms of specific results, they are however only applicable to the precise layup under investigation. Therefore to draw wider conclusions on the effect of wrinkles in various laminates a numerical study was conducted to assess the effect of stacking sequence on the first load drop due to delamination and also final failure for a level#3 wrinkle. Apart from the baseline [+45/90/-45/0]_{3S} configuration, two other lay-ups were chosen: [90/+45/-45/0]_{3S} and [+45/0/-90/-45]_{3S}. The choice of these additional lay-ups was motivated by the Virtual Crack Closure Technique (VCCT) study in reference [22]. In [22] the baseline layup was chosen as being the layup that had the highest free edge delamination strain, whilst still maintaining a 45° ply on the surface (as per industrial practise). The first additional layup had a higher free edge delamination strain (but not a 45° ply on the surface) and the second a lower free edge delamination strain. Further to this variation in ply ordering, instead of the baseline double ply blocking, a [+45/90/-45/0]_{6S} configuration was chosen, which is blocked at the sub laminate level. Such sub laminate level blocked specimens are known to exhibit reduced delamination growth [21] due to the smaller effective ply thickness. Figure 14(a) shows the comparison of model results for the different lay-ups in terms of cross-sectional tensile stress vs. displacement. The state of delamination in the ±45/90 interfaces after first load drop and
just before final failure in those lay-ups are shown in Figure 14(b). The $[90_2/45_2/-45_2/0_2]_{3s}$ shows a load drop due to first delamination occurring at a slightly higher stress level compared to the baseline, while the final failure stress levels are similar in both cases. This was in accordance with the previous VCCT analysis [22]. The $[+45_2/0_2/-90_2/-45_2]_{3s}$ lay-up did not show a distinct first load drop, and the final failure was predicted at a much higher stress-level. This can be explained by the fact that in this case the -45 plies constituted the central wavy layers instead of the 0 plies. The 45 plies, being not as stiff as the 0 plies, contained less stored elastic potential energy available for delamination growth and thus resulted in a lesser extent of delamination, as seen in Figure 14(b). The high final fibre failure stress is due to the fact that the load bearing 0 plies are located away from the central region and so do not have as high a wrinkle severity as the 0 plies in the other cases. The sub laminate level blocked specimen contained plies that were half the thickness of the baseline ply-blocked configuration. In this case, due to ply thickness being smaller, the total stored elastic potential energy in the plies was lower, also resulting in lower extent of delamination (Figure 14(b)). However, due to very little delamination in this case, even near complete failure, the wrinkled plies were constrained and prevented from straightening out, which upon further tensile loading caused the fibre failure stress to be exceeded locally in the wavy region relatively earlier than in the other cases.

4.2.5 Wrinkle severity vs. failure stress

A parametric study was carried out to gauge the effect of wrinkle severity on final failure stress levels. For this purpose, 3D FE models were developed with overall geometric dimensions remaining the same as in Figure 4, but with the wrinkle severity varying from 1° to 10° at intervals of 1°. This was achieved by changing the amplitude of the wrinkle in the models. The final failure predictions for this study, together with the three specific wrinkle levels tested, are shown in Figure 15. The model results for the tested wrinkle angles are also
plotted. It can clearly be seen that the final tensile failure stress levels for the laminates are a function of the wrinkle severity.

5 Conclusions

In this study, the effects of out-of-plane fibre wrinkling on the tensile damage and failure of fibre-reinforced composite laminates were evaluated experimentally and numerically. From the distinct nature of failure at the gauge section in all the cases tested, it can be confirmed that wrinkles act as local shear stress (through-the-thickness) concentrators. For multi-directional laminates, this effect is multiplied by the effect of in-plane shear stress localization near the laminate edge. It is the combinations of these two interlaminar shear stresses that bring about failure much earlier than what would be expected by fibre tensile damage. The monitoring capability of the high resolution camera was crucial during the test because it allowed careful observation of different modes of damage accumulation and their progression/interaction which was otherwise not possible from post failure inspections. The information obtained from the camera was also valuable because it helped to incorporate the correct damage mechanisms necessary to capture the failure in FE models. A comparison was also done numerically between different ply-stacking sequences while keeping the same wrinkle severity to estimate its effect on first load drop due to delamination and on final failure. It was observed that having off-axis plies (rather than axial 0 plies) in the most wavy regions reduces the propensity of delamination. However a reduced propensity for delamination is not always beneficial in terms of the maximum load bearing capacity, as is seen in the case of the sub-laminate blocked layup.

It was seen that the matrix crack model was crucial as the first load-drop in the tested configurations could not be captured correctly in the models in its absence Although the matrix crack model was reasonably mesh-size independent, it needed a fine mesh to confine
the damaged region within a narrow band which represented a ‘sharp’ crack. This resulted in better interaction with the cohesive elements and correctly initiated interfacial delamination. The mesh orientation bias of CDM models is a known fact, which causes damage progression to follow the mesh in multidirectional laminates. This aspect was minimised in the present work by a combination of a fine mesh and an in-plane aspect ratio close to unity in the wrinkle region of the mesh. A discrete damage modelling such as the phantom node method [23] may be used in future as an improvement which would overcome this limitation. The present work highlighted the importance of incorporating correct damage models and adequately capturing their interaction in the models, which resulted in accurate predictions of the global tensile failure behaviour in laminates containing embedded out-of-plane wrinkle.

6 Acknowledgements

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7 References


Figures

Figure 1. (a) Wrinkled plies in a curved composite part. (b) Geometric parameters associated with a wrinkle.

Figure 2. (a) Artificially inducing waviness using the strip-insertion method. (b) Lay-up profile of the laminate showing the location of inserted strips.
Figure 3. Measurement of wrinkle angle by tangent drawing technique.

Figure 4. FE mesh highlighting the wrinkle region developed using cosine approximation of waviness. Thickness change in certain locations in 90° plies (due to insertion of strips in test) is modelled accurately.
Figure 5. Comparison of wrinkle angle measured by tangent drawing technique and angle obtained using measured amplitude and wavelength and fitting to a cosine curve.

Figure 6. Traction components on the fracture plane after Pinho et al [10].
Figure 7. Mesh independency of the matrix crack model

Figure 8. Mixed-mode delamination map after Jiang et al [17].
Figure 9. (a) Load vs. crosshead displacement for a typical level#3 specimen. The first load-drop occurred between points ‘2’ and ‘3’. (b) Image sequence captured using high resolution video camera with the numerals indicating the corresponding points in the loading curve in (a).

Figure 10. Load vs. crosshead displacement for the level#3 specimen when the test stopped after first load drop. The CT scan image shows the localised delamination at the wrinkle region and the matrix cracks in the damaged structure after first load drop.
Figure 11. Comparison of test and model results for the first load-drop and final failure stress levels.

Figure 12. Location-wise comparison of damage from image from high resolution video camera (top) and FE model (bottom) at similar cross-sectional stress. The through-thickness deformation is magnified to show the shear delaminations. Numerals indicate similar locations.
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Figure 13. Stress vs. displacement of a level#3 specimen for three different mesh densities (with inset of delamination prediction at point ‘a’ from 423,984 element model)

Figure 14. (a) Stress vs. displacement of level#3 wrinkle severity for different lay-ups investigated in FE analysis. ‘D’ indicates delamination after first load drop, ‘F’ indicates complete failure due to fibre fracture. (b) Comparison of state of delamination in the wrinkle (shown in red) for various lay-ups.
Figure 15. Wrinkle severity vs final failure stress
Tables

Table 1. Undamaged thermo-elastic properties of IM7/8552 [16]

<table>
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<tr>
<th>$E_{11}$ (MPa)</th>
<th>$E_{22}$ (MPa)</th>
<th>$E_{33}$ (MPa)</th>
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<th>$\gamma_{13}$</th>
<th>$\gamma_{23}$</th>
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<th>$G_{13}$ (MPa)</th>
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Table 2. IM7/8552 properties for tension matrix crack model [7,11,18,22]

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<th>$Y_T^\infty$ (MPa)</th>
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<th>$S_L$ (MPa)</th>
<th>$G_N$ (N/mm)</th>
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Table 3. IM7/8552 properties for the delamination model [18]

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<th>$K_{II}$ (N/mm$^3$)</th>
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<th>$\sigma_{\text{limax}}$ (MPa)</th>
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Table 4. Wrinkle tension test results

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<th>CV (%)</th>
<th>final load-drop (fibre failure) mean (MPa)</th>
<th>CV (%)</th>
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