Modelling of perforation failure in fibre metal laminates subjected to high impulsive blast loading

E. Sitnikova a, Z.W. Guan a,⇑, G.K. Schleyer a, W.J. Cantwell a, b

a School of Engineering, University of Liverpool, Brownlow Street, Liverpool L69 3GQ, UK
b Aerospace Research and Innovation Center (ARIC), Khalifa University of Science, Technology and Research (KUSTAR), Abu Dhabi, United Arab Emirates

Abstract

Perforation failure of fibre metal laminate (FML) panels subjected to the localized high intensity blast loading is studied. The FMLs are based on various stacking configurations of aluminium alloy sheets and layers of woven glass fibre in a polypropylene matrix (GFPP) composite. Finite element models of the FMLs were created using the commercial software package Abaqus/Explicit, where the constitutive relationships and damage in the composite material were described through a user-defined subroutine. The composite was modelled as an orthotropically elastic material prior to damage initiation and the growth of subsequent damage was based on an instant failure rate-dependent model. The simulated deformation and failure modes in the FMLs were found to be in good agreement with published experimental data. For FMLs based on thin GFPP layers, a number of dynamic failure scenarios were captured, such as petalling, large tensile tearing and multiple debonding between the aluminium and GFPP layers. A high degree of correlation between simulated failure on the back face aluminium and the underlying GFPP damage modes was revealed. Finally, the transient behaviour of FML panels during blast loading was studied and discussed.

1. Introduction

Over the last decade, there has been a growing interest in studying the response of fibre metal laminate (FML) structures to blast loading. An FML is a hybrid engineering material, consisting of various stacking arrangements of metallic alloy and fibre reinforced composite layers. Glass fibre composite based FMLs, such as glass laminate aluminium reinforced epoxy (GLARE), are of a particular interest for the potential design of load-bearing structures. Being lightweight and having a relatively low cost, these hybrid materials can offer improved strength and fatigue properties compared to monolithic metallic plates and aramid and carbon reinforced FMLs (Vlot, 1996).

In recent years, there have been a number of studies investigating the impact resistance of FMLs based on various compositions and under different loading conditions. The experimental work and subsequent finite element analysis of ballistic perforation of GLARE FMLs carried out by Seyed Yaghoubi and Liaw (2013) showed that cross-ply composites dissipate more energy than unidirectional composites. The mechanism of energy dissipation during ballistic impact of fully-clamped GLARE panels was investigated by Fatt et al. (2003), where it was shown that most of the energy dissipation was due to panel bending, with the relative amount of absorbed energy being higher for thinner panels. In contrast, low velocity impact tests on glass fibre reinforced epoxy/ aluminium FMLs, carried out by Fan et al. (2011), showed that a substantial increase in resistance to perforation can be achieved by increasing the thickness of the composite layers in the FMLs. Experiments on the high-velocity impact response of self-reinforced polypropylene based FMLs by Abdullah and Cantwell (2006) suggest that significant energy dissipation results from the stretching during flexure of the aluminium layers, which deform independently of the composite plies.

One of the greatest challenges in modelling the dynamic response of FMLs is to choose an appropriate mathematical description of both the failure and damage evolution mechanisms in the composite material. Many composite failure models are based on continuum damage mechanics. In particular, Iannucci (2006) developed a simple progressive failure model for thin carbon composites using a thermodynamic maximum energy dissipation approach, which allows the user to control the rate of damage evolution. Hassan and Batra (2008) used deformation-dependent damage variables to represent different failure modes. A full 3D finite element model for thick-sectioned composites was developed by Gama and Gillespie (2011), in which dynamic effects were...
accounted for by assuming a logarithmic strain-rate dependence for the elastic and strength constants. The damage mechanics approach developed by Matzenmiller et al. (1995) for in-plane failure in a unidirectional fibre reinforced composite was used to characterize the softening behaviour after damage initiation.

Extensive experimental studies on blast failure in FMLs have been carried out by Langdon et al. (2007a,b). A variety of failure scenarios were observed in these tests, ranging from debonding between the aluminium and composite layers at low impulses, to perforation failure accompanied by petalling of the aluminium and rupture of the composite under more severe blast loading. Finite element modelling of FMLs subjected to low impulse blast loads has also been carried out (Karagiozova et al., 2010; Vo et al., 2012, 2013). In particular, numerical analyses conducted by Karagiozova et al. (2010) highlighted the importance of accurate modelling of the applied pressure, since the initial deformation phase is highly sensitive to its spatial distribution. Modelling of the full 3D composite constitutive behaviour of FMLs was conducted by Vo et al. (2012, 2013). The finite element model predictions in Vo et al. (2012) were shown to be in reasonable agreement with the experimental data (Langdon et al., 2007a), capturing both aluminium debonding and other types of damage.

The present work focuses on modelling perforation failure in FMLs based on different stacking configurations. An instant failure model has been developed to describe dynamic failure in the woven glass fibre propylene matrix (GPPM) composite. The relevant constitutive model, failure criteria and rate-dependence behaviour are implemented into Abaqus/Explicit through a user-defined subroutine. The perforation failure of various FMLs is simulated, capturing basic features, such as petalling failure of the back face aluminium layer, tensile tearing failure of the composite plies and complete perforation. It has been shown that the numerical models can be used to provide an accurate prediction of the blast resistance and failure modes of FMLs. The limitations of the current model are also briefly discussed.

2. Fibre metal laminates

The numerical model for predicting the response of fibre metal laminates is validated on the basis of experimental data obtained following blast tests at the University of Cape Town (Langdon et al., 2007a,b). The fibre metal laminates used in these tests were manufactured at the University of Liverpool. They comprised of layers of aluminium alloy 2024-O and a composite, formed from different numbers of woven GPPM plies. A thin layer of polypropylene film (Xiro 23,101) was placed between the aluminium sheet and the composite layer in order to ensure a high level of adhesion between the two materials.

For ease of reference, the same FML notation is used as that in the previous work (Langdon et al., 2007a,b). Namely, AXTVYZ signifies that the FML contains X aluminium sheets, Y = X – 1 composite blocks, with Z plies in each composite block. A comprehensive description of the experimental set-up, test techniques and FML manufacturing procedures can be found in references (Langdon et al., 2007a,b).

3. Material models

To describe the overall dynamic behaviour of a FML, mathematical models for its three constituent materials were used. The constitutive models for the aluminium and cohesive material are available in Abaqus (2010) and are also reported in Vo et al. (2012). Therefore, the modelling approaches for these materials are briefly outlined in Appendices A and B, respectively. The composite material model available in Abaqus has a number of limitations, which make it unsuitable for describing the dynamic response of a woven composite material. In particular, in Abaqus, the composite layers have to be modelled as shell elements and therefore through-the-thickness stresses and strains are neglected. For the thicker composites, however, the out-of-plane deformation components become important, especially when a large transverse compression load is involved. Next, the failure criteria used in Abaqus only apply to unidirectional composites. Finally, strain-rate effects must be accounted for when modelling the dynamic response of the material. Therefore, in order to adequately describe the constitutive mechanical behaviour and failure criteria of the composite, a full 3D woven composite constitutive model was developed and implemented into the VUMAT subroutine.

3.1. Constitutive equations for the glass fibre reinforced composite

The macroscopic constitutive response of the undamaged woven composite is commonly described by an orthotropic elastic material model (for example, see Iannucci, 2006; Gama and Gillespie, 2011). To account for through-the-thickness effects, the 3D material response is considered, for which the stress–strain relations are written as follows:

$$\begin{bmatrix}
\sigma_{11} \\
\sigma_{22} \\
\sigma_{33} \\
\sigma_{32} \\
\sigma_{31} \\
\end{bmatrix} =
\begin{bmatrix}
C_{11} & C_{12} & C_{13} & 0 & 0 & 0 \\
C_{21} & C_{22} & C_{23} & 0 & 0 & 0 \\
C_{31} & C_{32} & C_{33} & 0 & 0 & 0 \\
0 & 0 & 0 & G_{44} & 0 & 0 \\
0 & 0 & 0 & 0 & G_{55} & 0 \\
0 & 0 & 0 & 0 & 0 & G_{66} \\
\end{bmatrix} \begin{bmatrix}
e_{11} \\
e_{22} \\
e_{33} \\
e_{23} \\
e_{31} \\
e_{32} \\
\end{bmatrix},
$$

(1)

where the elastic stiffness constants are defined as:

$$C_{11} = E_1(1 - v_{23}v_{32})\Gamma,$$

$$C_{22} = E_2(1 - v_{31}v_{13})\Gamma,$$

$$C_{33} = E_3(1 - v_{12}v_{21})\Gamma,$$

$$C_{12} = C_{21} = E_1(v_{23}v_{31} + v_{32}v_{21})\Gamma,$$

$$C_{23} = C_{32} = E_2(v_{31}v_{13} + v_{12}v_{31})\Gamma,$$

$$C_{31} = C_{13} = E_3(v_{12}v_{23} + v_{21}v_{32})\Gamma,$$

$$G_{44} = G_{12},$$

$$G_{55} = G_{23},$$

$$G_{66} = G_{13},$$

where $\Gamma$ is defined as follows:

$$\Gamma = 1/(1 - v_{12}v_{21} - v_{23}v_{32} - v_{31}v_{13} - 2v_{12}v_{23}v_{31}).$$

(3)

In order to ensure symmetry in the stiffness matrix, the Poisson’s ratios should satisfy the following conditions:

$$\frac{E_2}{E_1} = \frac{v_{31}}{v_{13}}, \quad \frac{E_3}{E_1} = \frac{v_{23}}{v_{13}}.$$

(4)

The values used for these constants are given in Table 1.

In this work, a very simple composite failure model was used. It is based on an assumption of instant failure, i.e. the material fails once the appropriate failure criteria are satisfied, and no damage evolution is considered.

Failure in the 3D woven composite can be modelled using the quadratic stress-based criteria described in Gama and Gillespie (2011). Essentially, these are the generalizations of the failure criteria proposed by Hashin (1980) for a unidirectional composite. There are four failure conditions which are described below.

Tensile-shear failure in the warp fibre direction:

$$\frac{f_{tt}}{E_t} = \left(\frac{\sigma_{11}}{E_t}\right)^2 + \left(\frac{\sigma_{12}}{E_t}\right)^2 + \left(\frac{\sigma_{31}}{E_t}\right)^2 - 1 = 0, \quad \sigma_{11} > 0.$$

(5)

Tensile-shear failure in the weft fibre direction:
Composite material parameters (Karagiozova et al., 2010).

<table>
<thead>
<tr>
<th>ρ (kg m⁻³)</th>
<th>E₁, E₂ (GPa)</th>
<th>E₃ (GPa)</th>
<th>ν₁₂</th>
<th>ν₂₃, ν₁₃</th>
<th>G₁₂ (GPa)</th>
<th>G₁₃, G₂₃ (GPa)</th>
</tr>
</thead>
<tbody>
<tr>
<td>1800</td>
<td>13</td>
<td>4.8</td>
<td>0.1</td>
<td>0.3</td>
<td>1.72</td>
<td>1.69</td>
</tr>
</tbody>
</table>

\[
f_{22} = \frac{\sigma_{22}^2}{C_{12}} + \frac{(\sigma_{12})^2}{C_{12}} + \frac{(\sigma_{23})^2}{C_{12}} - 1 = 0, \quad \sigma_{22} > 0. \quad (6)
\]

Compressive failure in the warp fibre direction:

\[
f_{1c} = \frac{(\sigma_{11})^2}{C_{11}} - 1 = 0, \quad \sigma_{11} < 0. \quad (7)
\]

Compressive failure in the weft fibre direction:

\[
f_{2c} = \frac{(\sigma_{22})^2}{C_{22}} - 1 = 0, \quad \sigma_{22} < 0. \quad (8)
\]

Instant failure implies that once one of Eqs. (5)–(8) is satisfied, the material is assumed to fail and the element is removed from the mesh.

The values of damage variables corresponding to each of the failure modes listed above are as follows:

\[
d_{ab} = \begin{cases} 
0, & \text{if } f_{ab} < 0, \\
1, & \text{if } f_{ab} \geq 0, 
\end{cases} \quad (9)
\]

where \(ab = (1t, 1c, 2t, 2c)\), with the subscripts “t” and “c” denoting tensile and compressive failure, respectively. Consequently, when any one of the failure conditions (5)–(8) is satisfied, all stiffness components are reduced to zero, and damaged stiffness can be written as \(C'_{ij} = C_{ij}(1 - d_{11})(1 - d_{12})(1 - d_{22})(1 - d_{23}) = 0\).

3.2. Strain-rate dependence of the strength properties of the composite

Dynamic effects in the blast response of the composite are taken into account by introducing a strain-rate dependency in the composite strengths. A logarithmic dependence was considered similar to that used by Gama and Gillespie (2011) and Yen (2002), which can be written as follows:

\[
\tilde{S}(\dot{\varepsilon}) = \tilde{S}_0 \left(1 + R \ln \frac{\dot{\varepsilon}}{\dot{\varepsilon}_0}\right), \quad (10)
\]

where

\[
S = (S_{11}, S_{1c}, S_{2t}, S_{2c}, S_{12}, S_{23}, S_{13})^T,
\quad S'_0 = (S'_{11}, S'_{1c}, S'_{2t}, S'_{2c}, S'_{12}, S'_{23}, S'_{13})^T,
\quad \tilde{\varepsilon} = (\varepsilon_{11}, \varepsilon_{1c}, \varepsilon_{2t}, \varepsilon_{2c}, \varepsilon_{12}, \varepsilon_{23}, \varepsilon_{13})^T.
\]

Here \(S'_{11} = S'_{22} = 360\) MPa and \(S'_{2c} = S'_{5c} = 280\) MPa are the static in-plane tensile and compressive strengths, respectively, \(S'_{c} = 180\) MPa is through the thickness failure strength, and \(S'_{12} = S'_{13} = S'_{23} = 150\) MPa are the static shear failure strengths. These values are typical of those for glass fibre composites (see for example Fan et al., 2011). The values for the reference strain rate and the strain-rate constant were chosen to be \(\dot{\varepsilon}_0 = 1\) s⁻¹ and \(R = 0.2\), respectively.

The elastic moduli are considered to be strain-rate insensitive. A previous study of failure of FMLs with glass-based composites (McCarthy et al., 2004) has indeed shown that the in-plane elastic moduli are strain-rate insensitive, while the failure strengths and transverse modulus values exhibit a strain-rate dependence. However, due to the lack of data regarding the rate-sensitivity of the out-of-plane elastic constants, such rate-dependency was not taken into account in the current model.

3.3. Implementation of the composite material model in the user-defined subroutine

The VUMAT subroutine that describes the composite constitutive response and failure model was incorporated into the main Abaqus program. The subroutine is called at each time increment to perform numerical integrations for the composite. The values for the time and strain increments are calculated automatically through the Abaqus/Explicit main program. Strain increments are used to compute stress increment values and subsequently update the stress values from the previous step. A flowchart describing the implementation of the instant failure model in the VUMAT subroutine is presented in Fig. 1.

In order to check the validity of the model implementation, the constitutive response of the composite was initially studied, based on a single element model. Typical results of such analyses are presented in Fig. 2. The rate dependence of the strength \(S_{22}\) as given by Eq. (10) is plotted in Fig. 2(a). Stress–strain curves, obtained for uniaxial loading in the 2-direction at five different strain rates, are shown in Fig. 2(b). As can be seen, stress–strain dependence is linear up to the point of failure, at which point all the stiffness components are reduced to zero. Comparing the maximum stress values in Fig. 2 (b) with the appropriate strength values in Fig. 2(a), it can be seen that the stress values at failure are predicted accurately.

Fig. 2(c) shows the direct strains at \(\dot{\varepsilon} = 5557\) s⁻¹. As expected, the only positive strain component under uniaxial tension in the 2-direction is \(\varepsilon_{22}\). Further, the absolute values of the direct strains \(\varepsilon_{11}\) and \(\varepsilon_{33}\) are significantly smaller than that of \(\varepsilon_{22}\). The time history of the damage variable \(d_{22}\), at the same strain-rate is shown in Fig. 2(d). Here, it can be seen that the value of \(d_{22}\) changes from zero to unity at the moment that the stress values drop to zero. This is consistent with the assumption of the instantaneous failure
4. Finite element modelling of the FMLs

To simplify the numerical analysis, one quarter of the FML panel with appropriate symmetry and boundary conditions was modelled, as shown in Fig. 3. This is a reasonable simplification given the symmetric nature of the problem. Clearly, this assumption substantially reduces computational time and associated costs.

Within each FML configuration, there was a ±4% variation in panel thickness (Langdon et al., 2007a). To simplify modelling, the mean thickness of each lay-up was calculated, and used to model all the blast cases for that given configuration. The aluminium layer thickness of 0.6 mm and thermoplastic interlayer thickness 0.1 mm (assumed) were kept constant for all the FML lay-ups. In contrast to previous studies (Karagiozova et al., 2010; Vo et al., 2012), each ply in a composite block was modelled individually, with the general contact algorithm defining contact between two neighboring plies. The composite ply thickness was therefore calculated by subtracting the total thickness of the aluminium and the cohesive layers from the average FML thickness and dividing this value by the number of the composite plies.

The 3D finite element models of the aluminium layers and composite plies were created using 8-node solid elements with reduced integration (C3D8R), and the cohesive layers were meshed using COH3D8 elements. A finer mesh size of 1/2 mm was used in the central 60 x 60 mm area of the panel, while a biased mesh was generated outside this area. The choice for the mesh size was based on the mesh sensitivity study, which revealed that this mesh size ensures the satisfactory accuracy of the numerical results and convergence of the solution. This also confirms the results of the mesh sensitivity study carried out in the previous work on FE modelling of failure in FMLs under blast loading (Karagiozova et al., 2010; Vo et al., 2012).

A “rough friction” surface interaction was defined between the aluminium and composite layers. This was to accommodate the case where these surfaces come into contact after the cohesive layer had failed. Therefore, the areas of the aluminium and composite layers near the blast zone were defined as element-based
surfaces that include both exterior and interior faces. It is worth noting that at perforation failure, contact should be defined on both the exterior and interior faces of the contacting layers. Since the material erodes as it fails, the zone of contact extends to the interior elements, and an exterior surface interaction condition becomes insufficient in this case.

Element deletion was applied using the variable controlling the element deletion, a facility that is available in Abaqus/Explicit. This variable is activated in the VUMAT subroutine code when either of the failure criteria are satisfied. Details of the composite material erosion process and the element deletion procedure are given in Appendix C, where the failure of the centrally loaded single composite ply is considered.

4.1. Loading function

In the blast experiments (Langdon et al., 2007a,b), a high intensity loading condition was achieved by detonating a 30 mm diameter plastic explosive disc attached to the centre of a FML plate. In previous works on the numerical modelling of FML blast failure (Karagiozova et al., 2010; Vo et al., 2012), impulse equation was used to numerically model the pressure applied to the front face of FMLs. The total impulse was calculated as:

\[
I = 2\pi \int_0^\infty \int_0^{r_b} r P(r,t) \, dr \, dt,
\]

where \( I \) is a loading impulse which was measured in the experiment, \( t \) is time and \( r \) is the distance from the centre of the FML panel. Next, an approximation of the pressure load \( P(r,t) \) was employed, and it was treated as a function of time and distance, namely (Karagiozova et al., 2010):

\[
P(r,t) = P_1(r)P_2(t).
\]

In this paper, a similar approach to model the loading function was used, however the spatial and time pressure distribution functions \( P_1(r) \) and \( P_2(t) \) has been modified. An explicit expression for \( P_1(r) \) has not been changed, and it is written as follows:

\[
P_1(r) = \begin{cases} 
  p_0 & \text{if } r \leq r_0, \\
  p_0 e^{-k(r-r_0)} & \text{if } r_0 < r \leq r_b, \\
  0 & \text{if } r > r_b,
\end{cases}
\]

where \( r_0 = 0.011 \text{ m} \) is the radius of the area in the centre of FML with constant pressure, \( k = 130 \text{ m}^{-1} \) is the decay constant, and \( r_b = 0.1 \text{ m} \) is the distance from the centre of the plate at which impulse values can be considered negligible. The spatial pulse distribution is shown in Fig. 4(a).

Note that in Karagiozova et al. (2010) and Vo et al. (2012), quantity \( r_0 \) was equal to the radius of the explosive disk \( r_e \). However, by observing experimental data it is easy to see that the size of perforation hole is in most of the cases smaller than the diameter of the explosive. Moreover, in later work on blast failure of sandwich panels (Langdon et al., 2013) a more extensive study of loading function was carried out. It suggests that pressure is not constant in the area bounded by the disk of explosive, and the constant pressure is achieved over area of smaller radius.

The time history of the pressure pulse is also similar to the one proposed by Langdon et al. (2013). The pressure increases linearly over a short time interval \( t \), prior to its exponential decay, as shown in Fig. 4(b). This gives a more realistic representation of the blast load, giving the material time to react to the applied pressure, rather than provide an instant response. The pulse time history can therefore be written as:

\[
P_2(t) = \begin{cases} 
  t/t_s & \text{if } t \leq t_s, \\
  e^{-k(t-t_s)/t_0} & \text{if } t > t_s,
\end{cases}
\]

where \( t_s = 5 \mu s \) and \( t_0 = 20 \mu s \) are the time constants. This results in pulse duration which is also similar to the one proposed by Langdon et al. (2013).

The pressure amplitude \( p_0 \) can be calculated by substituting Eqs. (14) and (15) into Eq. (12) and re-arranging the terms after integration.

5. Results and discussion

5.1. Transient behaviour during failure

One of the big advantages of finite element modelling of blast events is that it allows studying the transient response of the structure, as it is difficult to capture and monitor transient behaviour in real-life blast experiments. Consider gradual failure of A2T12 FML panel at \( l = 11.48 \text{ Ns} \) as shown in Fig. 5, where the through-thickness velocity contour plots are presented at ten instances of time. Initially at \( t = 1 \mu s \) there is a rapid increase in velocity in the centre of the plate. The radius of the area with high element velocity is equal \( r_0 \), which is the radius of constant pressure area as described in Section 4.1. The velocity rapidly increases and propagates through the thickness of the panel, and at \( t = 4 \mu s \) high velocity values are reached on the back face of the panel as well. At \( t = 18 \mu s \) there are obvious deformations on the panel, yet the deformations are confined to the centre of the panel. At the same time, tearing on the front face aluminium sheet is observed along with the failure of composite around the constant pressure zone. Immediately after that, at \( t = 20 \mu s \) the back face aluminium sheet is perforated, too. This is accompanied by the rapid drop of the velocity in FML panel, while fragmented central part still maintains high velocity. From this instant a continuous reduction of velocity in the panel is observed, and in order to have a clear picture of velocity changes within the FML panel, the fragmented part is not displayed in further images.

Subsequently, cracks growing from the perforation zone outwards appear both in aluminium sheets and composite plies, and
they are visible in the contour plot at $t = 70 \mu s$. In contrast to the previous plot, non-zero velocity values are detected near the edges of the panel indicating presence of small oscillations in this region. At $t = 0.1 \text{ ms}$ the cracks become even more pronounced, and the deformation becomes less centralized as it starts spreading towards the edges of the panel. At $t = 0.2 \text{ ms}$ the debonding between the composite layer and front face aluminium sheet is clearly seen, and the largest transient deformation of the panel is reached at $t = 0.7 \text{ ms}$. At this point the debonded composite layers around the perforation zone “flap” backwards causing tearing and petalling of the back face aluminium layer.

At $t = 2.2 \text{ ms}$, transient deformations are recovered and at this point the permanent deformation state is achieved. However, there is still some residual non-zero velocity present in the panel, which is further reduced by $t = 4 \text{ ms}$.

Though there can be a small variation in time values when each stage of failure process takes place, the failure scenario described is typical for all FML panels considered in this paper.

5.2. Qualitative comparison of perforated FML panels with numerical predictions

The numerical predictions of perforation failure in the blast-loaded FMLs were compared with the corresponding experimental results (Langdon et al., 2007a,b). In the modelling work discussed here, damage patterns in various FML lay-ups are presented in the form of digital images of the front and back faces and cross-sections through the centre of the panel. The numerical data determined in the experiments were the blast impulse and the residual displacements of the front and back faces, for those cases where the FMLs did not fail in a perforation mode. Since the present study concentrates on modelling perforation failure, the accuracy of the predictions of the mathematical model was mainly assessed by comparing the available experimental images with the calculated displacement contour plots. To improve clarity and facilitate comparisons, the quarters of FML panels were reflected with respect to their planes of symmetry using an appropriate Abaqus postprocessing tool. The calculations were carried out over a time period of 4 ms, allowing the panel to have sufficient time to recover the large transient deformations.

Perforation failures in the A2T12 panels subjected to various blast loadings are shown in Figs. 6 and 7. The most distinctive features of these FMLs are the rupture and outward petalling of the composite and aluminium layers around the perimeter of the central perforation hole. This behaviour was also observed following the experiments. It can be seen from the contour plots that the diameter of the petalling region as well as the number of petals in back face aluminium sheet increase as the impulse increases (Fig. 7). This is also consistent with the experimental results.

The A2T14 FML panel was subjected to a lower impulse, $I = 6.63 \text{ N s}$, which resulted in five petals in back face aluminium sheet around the perforation hole. Hence the number petals predicted by the model, four, is in good agreement with the experimental data. By comparing cross-sections in Fig. 8(a) and (c) it is easy to see that the calculations capture the debonding between the aluminium sheets and the composite layer, though the size of debonding is overestimated in both panel faces.

Failure in the A3T22 panel was less severe compared to that in the A2T12 laminate, due to the greater number of layers and the lower applied impulse. Comparison of the experimental images and numerical results for this FML configuration is given in Fig. 9. Here, some petalling as well as the presence of a diamond-shaped delamination region can be seen around the central perforation hole. Back face debonding was accurately captured by the calculations, however the model suggests multiple debonding between the layers of aluminium and composite which was less clearly observed in the experiment.
On the other hand, multiple debonding predicted for the panel A3T24, which has the same number of aluminium layers yet thicker composite blocks, shows a better agreement with the experimental data. Fig. 10 shows failed cross-sections for the A3T24 laminate subjected to two impulses. Both panels exhibit a substantial amount of delamination, along with some petalling of the back face aluminium panel. A comparison of the associated failed back faces is shown in Fig. 11. The experimental perforation pattern and the deformation mode are predicted reasonably well, in particular, the size of the total perforation hole increases with increasing impulse, and the calculation predicts eight petals in back face aluminium sheet at $I = 16.19 \text{ Ns}$, which is the same number as observed in the experiment.

Figure 6. Comparison of the predicted displacement contour plots with the experimental data (Langdon et al., 2007b) for A2T12 FML panels over a range of the blast impulses.

$\text{Fig. 6.} \text{ Comparison of the predicted displacement contour plots with the experimental data (Langdon et al., 2007b) for A2T12 FML panels over a range of the blast impulses.}$

Failure in those FMLs based on a large number of layers was more complex, due to multiple debonding between the aluminium and composite layers. Whilst little debonding of the aluminium layers was observed near the central blast area in panel A4T32, as can be seen in Fig. 12, the model over-estimates the level of debonding. Compared to panel A4T32, laminate A4T34 has the same number of aluminium layers, but thicker composite blocks, i.e. four plies in each block. For this configuration, the level of debonding between the aluminium plates and the composite blocks is more severe, which is evident both in the test panel and the numerical simulation except at the skin layers, as shown in Fig. 13.

Figure 7. Comparison of the back face petalling failure predicted by the numerical model with the experimental data (Langdon et al., 2007a) for A2T12 FML panels over a range of the blast impulses.

$\text{Fig. 7.} \text{ Comparison of the back face petalling failure predicted by the numerical model with the experimental data (Langdon et al., 2007a) for A2T12 FML panels over a range of the blast impulses.}$

Finally, for those FMLs with the greatest number of aluminium layers, A5T42, extensive debonding between the constituent layers
was observed for both the numerical model and the experiment, as can be seen from a comparison of the panels in Fig. 14. Also, the back face perforation zone for A4T34 and A5T42 is more centralized and has no petals around the perforation hole. This was also predicted by calculations.

5.3. Quantitative comparison of the calculated failure zone sizes with the experimental data

Since there is limited experimental data available for assessing the quantitative accuracy of the finite element model in the case of perforated FML panels, in order to present some quantitative validation the diameters of perforation holes in the front and back face aluminium sheets were compared. The measurements were taken from digital images as shown in Fig. 15 for both FE and experimental ones. Essentially, the distance between the two closest points on the perforated back face and front face aluminium sheets were measured along the line of cross-section. These data are presented in Table 2.

The above comparisons show that the numerical model provides the most accurate predictions for A2T12 panel, which is the thinnest FML considered, overestimates back face failure of thicker panels.
A2T14 and A3T22 panels, overestimates front face failure for A3T24, A4T32 and A4T34 panels and overestimates both for the thickest A5T42 panel.

Given complexity of the problem, the number of material models and parameters involved, uncertainty in defining loading function, etc., it is fair to say that these results are reasonable for the first approximation to model a multistage blast failure process in a FML panel. The failure of all the FML components is strongly interlinked, in particular, overestimation of aluminium failure might imply that aluminium strength and yield parameters are at the lower end of the properties. However, the aluminium failure is related to the failure of underlying composite as described in the discussion of transient behaviour in Section 5.1. Next, the debonding between the panels could be reduced by increasing strength and damage evolution parameters of the cohesive. Yet this would make top debonding of the back face aluminium, which is already underestimated in thicker panels, even more difficult. Also, if a cohesive layer becomes stronger than the composite and the latter one fails first, it can cause severe convergence difficulties in calculations due to the excessive distortion of cohesive elements. Finally, the assumption of instantaneous failure underestimates the load-carrying capacity of the composite layers, and hence so does the overall FMLs blast resistance.

**Fig. 11.** Comparison of the predicted back face failure pattern with the experimental data (Langdon et al., 2007a) for the FML panel A3T24.

**Fig. 12.** Comparison of the predicted displacement contour plots with the experimentally captured (Langdon et al., 2007b) failure of laminate A4T32 at $t = 10.3$ Ns ($P_0 = 642$ MPa).

**Fig. 13.** Comparison of the predicted displacement contour plots (in m) with the deformed cross-section of FML lay-up A4T34 from (Langdon et al., 2007b) at $t = 16.63$ Ns ($P_0 = 1138$ MPa).
Therefore, the natural development of a blast failure model of FMLs would be to introduce damage evolution into the composite failure model, which will be a subject of the further research. It is anticipated that including progressive damage in the composite failure model would suppress the premature brittle failure in the composite and improve the overall numerical predictions.

At the same time, despite some limitations in the predictions associated with the assumption of instant failure in the composite, the model captures well the important dynamic failure features in the FMLs, including perforation, petalling, extensive tearing and debonding. Another advantage of the current model is the relatively simple implementation of the user-defined code into the main commercial program, which does not substantially slow down the numerical integrations. This evidence suggests, the current approach can be used to estimate the onset of failure as well as the final fracture modes in FMLs subjected to the blast loading.

6. Concluding remarks

Finite element modelling of perforation failure in GFPP/aluminium FMLs based on various stacking configurations has been carried out using the commercial code Abaqus/Explicit. The 3D rate-dependent instant failure model was developed and implemented into the main program as a user-defined subroutine to describe the blast response of the woven GFPP composite, whilst appropriate in-built Abaqus material models were used for modelling the aluminium sheets and the cohesive layers.

Failure development and transient behaviour of FML panels during blast loading were discussed, and different stages of failure process were described. In particular, a correlation between failure of the back face aluminium layer and neighbouring composite fracture has been revealed.

The finite element predictions have been compared with the published experimental results. The comparisons indicate that the material constitutive models and failure criteria well capture a number of high strain-rate failure features in the FMLs, such as petalling, multiple debonding and perforation failure. This implies that instant failure model can be a useful tool in assessing the blast failure in FMLs. It is anticipated that the introduction of damage evolution will provide a more realistic response of woven

Table 2

<table>
<thead>
<tr>
<th>FML panel</th>
<th>Impulse, N s</th>
<th>Front face failure, mm</th>
<th>Difference, %</th>
<th>Back face failure, mm</th>
<th>Difference, %</th>
</tr>
</thead>
<tbody>
<tr>
<td>A2T12</td>
<td>10.34</td>
<td>47</td>
<td>63</td>
<td>34</td>
<td>82</td>
</tr>
<tr>
<td>A2T14</td>
<td>6.63</td>
<td>28</td>
<td>27</td>
<td>–4</td>
<td>58</td>
</tr>
<tr>
<td>A3T22</td>
<td>8.50</td>
<td>25</td>
<td>27</td>
<td>9</td>
<td>44</td>
</tr>
<tr>
<td>A3T24</td>
<td>12.08</td>
<td>27</td>
<td>42</td>
<td>58</td>
<td>59</td>
</tr>
<tr>
<td>A4T32</td>
<td>10.30</td>
<td>18</td>
<td>31</td>
<td>72</td>
<td>–</td>
</tr>
<tr>
<td>A5T42</td>
<td>14.70</td>
<td>18</td>
<td>34</td>
<td>89</td>
<td>37</td>
</tr>
</tbody>
</table>
composites subjected to high impulsive blast loading and further improve the numerical predictions. This will be investigated in a future study.

Acknowledgements

The authors would like to thank the Leverhulme Trust (UK) for providing financial support for this research. The authors would also like to acknowledge the use of the UK National e-Infrastructure Service in carrying out this work.

Appendix A. Aluminium failure model

To model failure in the aluminium layers, the Johnson–Cook plasticity and failure criteria were used, as described in Abaqus (2010).

Johnson–Cook rate-dependent plasticity (temperature effects are neglected in the current study) is defined as follows:

\[
\sigma = [A + B(\dot{\varepsilon}_p)^n] \left[ 1 + C \ln \left( \frac{\dot{\varepsilon}_p}{\dot{\varepsilon}_0} \right) \right],
\]

where \( A \) is the yield strength for the Al2024-O alloy. The values for the material parameters \( B \) and \( n \) where chosen to ensure an ultimate tensile strength of 210 MPa.

The Johnson–Cook dynamic failure model uses the damage parameter \( \omega \), which is defined as:

\[
\omega = \sum \left( \Delta \varepsilon_{pl}^{p} / \dot{\varepsilon}_p \right)^3.
\]

where \( \Delta \varepsilon_{pl} \) is an equivalent plastic strain increment, and \( \dot{\varepsilon}_p \) is the rate-dependant failure strain. Its explicit expression is given as follows:

\[
\dot{\varepsilon}_p = \left[ d_1 + d_2 \exp(d_3 p/q) \right] \left[ 1 + d_4 \ln \left( \Delta \varepsilon_{pl} / \dot{\varepsilon}_0 \right) \right].
\]

According to this approach, failure occurs when the damage parameter \( \omega \) exceeds unity. For Al2024-O alloy, values for the parameters \( d_1 \) to \( d_4 \) in Eq. (A.3) are not available in the literature, therefore parameters for Al2024-T3 from Lesuer (2000) were used in the current modelling.

The material parameter values for the aluminium alloy are summarized in Table A.1.

Appendix B. Constitutive model for cohesive failure

Cohesive elements were used to model delamination failure between the aluminium sheets and composite plies. A number of constitutive models for cohesive elements is available in Abaqus (2010). In the current analysis, the response of the cohesive layer is described in terms of traction versus separation.

Damage initiation was described by a quadratic nominal stress criterion, as follows:

\[
\left( \frac{t_{n}}{t_{n}^0} \right)^2 + \left( \frac{t_{s}}{t_{s}^0} \right)^2 + \left( \frac{t_{v}}{t_{v}^0} \right)^2 = 1.
\]

Here \( (x) = (|x| + x)/2 \) is a Macaulay bracket, \( t_n, t_s \) and \( t_v \) are the current normal and shear stresses in the cohesive element, and \( t_{n0}, t_{s0} \) and \( t_{v0} \) are the maximum nominal stresses in the normal and shear directions, respectively.

Damage evolution was described using a power law as follows:

\[
\left( \frac{G_n}{G_n^c} \right) + \left( \frac{G_s}{G_s^c} \right) + \left( \frac{G_t}{G_t^c} \right) = 1,
\]

where \( G_n, G_s \) and \( G_t \) denote the work done by the tractions and their conjugate relative displacements in the normal, first, and second shear directions and \( G_n^c, G_s^c \) and \( G_t^c \) are their associated critical fracture energies.

The cohesive interface parameter values are given in Table B.1. These values are based on those provided in Shi et al. (2012), where the values for the damage initiation and evolution parameters were increased into avoid too extensive debonding between aluminium and the composite.

Appendix C. Element erosion in a single composite ply

A typical scenario for deletion of failed elements and the erosion of composite can be explained based on a single composite ply FE model. As with the FML panels, one quarter of a ply with appropriate symmetry and boundary conditions was modelled, and a loading function, as described in Section 4.1, was applied to the central area of the ply. Fig. C.1 shows the stress distributions over the 40 × 40 mm area in the centre of the ply, which cover the Von Mises stress and \( \sigma_{11}, \sigma_{22} \) and \( \sigma_{12} \) stress components at four different time intervals.

It is evident that there is no failure in the ply up to \( t = 14 \) μs and prior to the failure (for example at \( t = 12 \) μs), a substantial increase in both direct stresses \( \sigma_{11}, \sigma_{22} \) and in–plane shear stress \( \sigma_{12} \) is predicted near the centre of the ply. The contour plot regions with high stress values are marked on with a red colour. Initial failure at \( t = 14 \) μs occurs in an area where the highest values of the shear stress occur, which indicates that at this instance the shear stress, \( \sigma_{12} \), contributes significantly to failure initiation, as described by the failure criteria (5) and (6) in Section 3.1.

Shortly after initial failure, e.g. at \( t = 32 \) μs, the failure mechanisms change, and damage begins to propagate along axes of the
symmetry of the ply. The stress concentrations near the crack tips in the appropriate directions are clearly visible on the contour plots of the direct stresses $\sigma_{11}$, $\sigma_{22}$. In contrast, the shear stress becomes distributed over a relatively large area. Therefore, in this case the largest contribution to the failure conditions (5) and (6) results from the direct stresses $\sigma_{11}$ and $\sigma_{22}$, respectively. This behaviour is maintained until $t = 360 \mu s$, when all of the stress components drop below the strength values and no more elements are deleted.

References


