Ballistic impact behaviour of woven fabric composite: Finite element analysis and experiments

V A Phadnis, K S Pandya, N K Naik, A Roy and V V Silberschmidt

1 Wolfson School of Mechanical and Manufacturing Engineering, Loughborough University, Loughborough, LE11 3TU, Leics, UK
2 Aerospace Engineering Department, Indian Institute of Technology, Powai, Mumbai - 400076, India

E-mail: v.a.phadnis@lboro.ac.uk

Abstract. A mechanical behaviour of plain-weave E-glass fabric/epoxy laminate composite plate exposed to ballistic impact is studied using a finite-element (FE) code Abaqus/Explicit. A ply-level FE model is developed, where a fabric-reinforced ply is modelled as a homogeneous orthotropic elastic material with potential to sustain progressive stiffness degradation due to fiber/matrix cracking, and plastic deformation under shear loading. The model is implemented as a VUMAT user subroutine. Ballistic experiments were carried out to validate the FE model. A parametric study for varying panel thickness is performed to compare impact resistance of the studied composite.

1. Introduction

In recent years, woven-fabric polymer-matrix composites (PMCs) have been used increasingly in defence-related applications due to their higher energy absorption, high through-thickness stiffness and strength properties. Additionally, their shape and properties can be tailored to meet the needs of a specific application. High-performance fibers such as carbon, boron and Kevlar are of interest for military and aerospace applications of composites primarily due to their resilience at high temperatures and corrosion resistance in comparison to traditional structural materials.

In ballistic impact, PMCs retard a projectile by absorbing its kinetic energy through different mechanisms such as deformation, delamination, and shear between layers. The condition for perforation, also called the ballistic limit velocity \( V_{50} \) is the most important factor for design of a suitable protective structure [1]. In this regard, significant research has been carried out on the behavior of composite materials under ballistic impact loading. Few studies can be mentioned: Zhu et al. [2] investigated the response of woven Kevlar/polyester laminates of varying thicknesses to quasi-static and dynamic penetration by cylindroconical projectiles. Ballistic limits were also determined and terminal velocities measured. It was reported that deliberately introduced delamination and changes in the volume fraction of fibres did not result in significant changes in the impact resistance. Some studies revealed that a damage pattern for dynamic loading was significantly different from that in the corresponding quasi-static penetration conditions. Cheng et al. [3] developed a model for high-
velocity impact of thick composites based on a continuum orthotropic constitutive behavior with stress-based failure criteria and a simplified degradation model of failure. The model was implemented into a hydrodynamic finite element code. Punching, fiber breakage, and delamination were the major energy-absorbing mechanisms of the penetration processes. Silva [4] carried out experiments to study effect of ballistic impact on Kevlar-29 impacted with simulated fragments. Numerical modelling was used to obtain an estimate for the limit perforation velocity ($V_{50}$) and simulate failure modes and damage. Good correlation between results of computational simulation and experimental data was reported, both in terms of deformation and damage of the laminates. Naik and Doshi [5] presented the ballistic impact behavior of typical woven fabric E-glass/epoxy thick composites analytically. It was reported that shear plugging was the major energy-absorbing mechanism.

Numerical models for predicting the performance of woven fabric composites have been the subject of interest for many years. However, due to the complexity involved in describing the response of fabric panels to ballistic impact, most models attempt to provide the most acceptable trade-off in performance analysis. To model a woven fabric down to a level of individual yarn crossovers would certainly be preferred in order to study frictional and crimping effects, but such studies are computationally impracticable. The complexity of the problem has forced researchers to accept certain simplifying assumptions to make it computable.

The work presented here focuses on the finite-element analysis of a ballistic impact response of plain-weave E-glass fabric/epoxy composite using the general-purpose finite-element software ABAQUS/Explicit [6]. The experimental details are discussed first, followed by a physics-based finite-element model that accounts for both delamination and in-plain failure of fabric-reinforced composite plies. Next, the obtained numerical results are compared with the experimental data and followed by conclusions.

2. Experimental studies

Experimental studies were carried out using a single-stage gas-gun-operated ballistic-impact test facility shown in figure 1. The material studied was commercially available plain-weave E-glass/epoxy fabric composite. The specimens were prepared in a way to obtain symmetric balanced cross-ply laminates of dimension 125 mm $\times$ 125 mm with thicknesses of 2.5, 3, 4.5 and 5 mm. The mass of the projectile of diameter 6.36 mm made of hardened steel was 6.42 g. Experimental studies were carried out on at least 6 specimens for each impact condition. Further experimental details are provided in [1].

![Figure 1. Ballistic impact test facility [1]](image-url)
3. Constitutive model for fabric-reinforced composites

A constitutive material model representing the mechanical response of the plain-weave glass fabric/epoxy composite at ply level was developed. Each ply was modelled as a homogeneous orthotropic elastic material with the potential to sustain progressive stiffness degradation due to fiber/matrix cracking and plastic deformation under shear loading. The model was implemented as a material subroutine, VUMAT in ABAQUS [6]. Its main details are discussed below.

3.1. Elastic continuum-damage-mechanics model

The fibre directions of woven fabric reinforcement are assumed to be orthogonal. The constitutive stress-strain relations are formulated in a local co-ordinate system with its base vectors aligned with fibre directions. The in-plane elastic stress-strain relations are given according to orthotropic elasticity with damage [7].

\[
\begin{bmatrix}
\varepsilon_{11} \\
\varepsilon_{22} \\
\varepsilon_{12}^d
\end{bmatrix} =
\begin{bmatrix}
\frac{1}{(1 - d_1)E_1} & -\frac{\nu_{12}}{E_1} & 0 \\
-\frac{\nu_{21}}{E_2} & \frac{1}{(1 - d_2)E_2} & 0 \\
0 & 0 & \frac{1}{(1 - d_{12})2G_{12}}
\end{bmatrix}
\begin{bmatrix}
\sigma_{11} \\
\sigma_{22} \\
\sigma_{12}^d
\end{bmatrix},
\]

(1)

where \( \varepsilon = (\varepsilon_{11}, \varepsilon_{22}, \varepsilon_{12}^d)^T \) is the elastic stain vector; \( \sigma = (\sigma_{11}, \sigma_{22}, \sigma_{12}^d)^T \) is the stress vector; \( E_1 \) and \( E_2 \) are the Young’s moduli in the principal orthotropic directions; \( G_{12} \) is the in-plane shear modulus; \( \nu_{12} \) and \( \nu_{21} \) are the principal Poisson’s ratios; \( d_1 \) and \( d_2 \) are the damage parameters associated with the fibre fracture along the principal orthotropic directions; and \( d_{12} \) is the damage parameter associated with the matrix micro-cracking due to in-plane shear deformation. The damage parameters vary between 0 and 1 and represent the stiffness degradation caused by micro-damage in the material. The material properties were taken from [1] owing to the similarity of specimens.

3.2. Elastic-plastic shear model

The in-plane shear response is dominated by a non-linear behavior of the matrix that exhibits both stiffness degradation due to matrix micro-cracking and plasticity [8]. The elastic part of the shear response was calculated using (1). Here, the plastic shear response of the material is considered. The matrix response may be inelastic due to extensive cracking or plasticity. This leads to permanent deformations in the ply upon unloading. To account for these effects, a classical plasticity model with an elastic domain function and a hardening law, which is applied to the effective stresses in the damaged material, is used. The elastic domain function \( F \) is given by

\[
F = |\tilde{\sigma}_{12}| - \tilde{\sigma}_0(\tilde{\varepsilon}^{pl}) \leq 0 .
\]

(2)

The hardening law is of the form:

\[
\tilde{\sigma}_0(\tilde{\varepsilon}^{pl}) = \tilde{\sigma}_{y0} + C(\tilde{\varepsilon}^{pl})^p ,
\]

(3)

where \( \tilde{\sigma}_{y0} \) is the initial effective shear yield stress; \( C \) and \( p \) are coefficients; and \( \tilde{\varepsilon}^{pl} \) is the equivalent
plastic stain due to shear deformation. The values of $\sigma_y$, $C$ and $p$ are listed in Table 2.

4. Description of the finite element model

4.1. Intralaminar response of the plate

The in-plane response of the fabric plies was modelled using the constitutive model described. The elasticity constants include the Young’s moduli in the fiber directions, the principal Poisson’s ratio, and the in-plane shear modulus. The damage initiation coefficients account for tensile and compressive strengths along the fiber directions and shear strength at the onset of shear damage. The values of the user material constants and damage initiation coefficients are given in Table 1. The damage-evolution coefficients are represented by tensile and compressive fracture energies per unit area along the fiber directions and by parameters $\alpha_{12}$ and $d_{12\text{max}}$. We assume $\alpha_{12} = 0.15$ and $d_{12\text{max}} = 1$, in our simulations. The fracture energies can be determined through the testing procedure described in [9].

| Parameter                                      | Variable | Value  |
|-----------------------------------------------|----------|--------|
| Longitudinal modulus (GPa)                    | $E_{11}$ | 12.5   |
| Transverse moduli (GPa)                       | $E_{22} = E_{33}$ | 11.8   |
| Principal Poisson’s ratio                     | $\nu_{12}$ | 0.169  |
| Shear moduli (GPa)                           | $G_{12} = G_{23} = G_{31}$ | 3.5    |
| Longitudinal tensile strength (MPa)           | $X_{1+}$ | 322    |
| Longitudinal compressive strength (MPa)       | $X_{1-}$ | 204    |
| Transverse tensile strength (MPa)             | $Y_{1+}$ | 290    |
| Transverse compressive strength (MPa)         | $Y_{1-}$ | 200    |
| In-plane shear strength (MPa)                 | $S$      | 39     |
| Mode-I fracture toughness (J/m$^2$)           | $G_{IC}$ | 504    |
| Mode-II fracture toughness (J/m$^2$)          | $G_{II}$ | 1566   |
| Ultimate failure strain                       | $\varepsilon_{\text{max}}$ | 4.2%   |

The critical values of the fracture energies along the fiber direction 2 were calculated so that the ratios $G_f^{2+} / G_f^{1+}$ and $G_f^{2-} / G_f^{1-}$ were approximately equal to the ratios $Y_{2+} / X_{1+}$ and $Y_{2-} / X_{1-}$, respectively.
Table 2. Shear plasticity coefficients [11]

| Parameter                                           | Variable | Value |
|-----------------------------------------------------|----------|-------|
| Initial effective shear yield stress (MPa)          | $\bar{\sigma}_{\gamma}$ | 150   |
| Hardening coefficient in equation (3)               | $C$      | 1125  |
| Exponent in equation (3)                           | $p$      | 1.05  |

4.2. Interlaminar response of plate

In order to account for adhesive bonds between adjacent plies and model the delamination phenomenon, a generalized traction-separation law was used. Damage initiation at the interfaces was defined using a quadratic nominal-stress criterion, according to which damage was assumed to initiate when the quadratic interaction function involving the stress ratios reaches a value of one [10]:

$$
\left( \frac{t_n}{\sigma_n^0} \right)^2 + \left( \frac{t_s}{\sigma_s^0} \right)^2 + \left( \frac{t_t}{\sigma_t^0} \right)^2 = 1 ,
$$

(4)

where $t = \{t_n, t_s, t_t\}$ is the nominal traction vector. The interface damage-initiation properties used in the present analysis are listed in Table 3. These properties are usually difficult to determine experimentally with good accuracy; therefore, they can be used as calibration parameters, if required. In the present work, the variation of the damage initiation parameters within 15% from the typical values given in Table 3 did not influence significantly the overall analysis results. It should be noted that experimental determination of material properties typically yields a variation of ~15%. Progression of damage at the interfaces was modelled using a critical mixed-mode energy behaviour based on the Benzeggagh–Kenane (BK) law [7]:

$$
G_n^c + (G_s^c - G_n^c) \left( \frac{G_S}{G_T} \right)^\eta = G^c ,
$$

(5)

where $G_S = G_n + G_s$ and $G_T = G_n + G_s + G_s$; $G_n, G_s$ and $G_t$ refer to the work done by the normal and the first and the second shear forces acting in the interface, respectively; $G_n^c$ and $G_s^c$ are critical fracture energies required to cause failure in normal and shear directions, respectively. $G^c$ is the total critical mixed-mode fracture energy and $\eta$ is the power coefficient taken to be 2.284. After damage initiation, its evolution until failure was modelled using exponential softening law [7].

Table 3. Interface damage-initiation properties

| Parameter                                           | Variable | Value |
|-----------------------------------------------------|----------|-------|
| Maximum nominal stress in normal mode (MPa)         | $t_n^0$  | 35    |
| Maximum nominal stress in shear directions (MPa)     | $t_s^0 = t_t^0$ | 65    |

4.3. Element deletion

In simulations, failed continuum shell elements were removed from the model to prevent excessive distortion and premature termination of the analysis. In the present work, damage-based element
deletion is combined with a deformation-based deletion criterion. The damage-based element deletion was activated when any damage variable along the fiber directions or the equivalent plastic strain due to shear deformation reached a maximum specified value. The deformation-based deletion criterion was used when either the tensile or compressive principal logarithmic strain reached its maximum or minimum specified value, respectively. Additionally, detached composite fragments were deleted when they moved far away from the impact zone to prevent non-physical numerical distortions.

4.4 Loading and boundary conditions
A schematic of the developed FE model is shown in figure 2. Both woven fabric plate and bullet were modelled as 3D continuum deformable solids. The dynamic explicit solver was used in simulations to account for the time-dependent loading and the complex interaction between the bullet and the fabric composite plate. A 3 mm thick symmetric cross-ply laminate in this FE model consisted of 12 plies with an individual ply thickness of 0.25 mm. The local co-ordinate systems were defined to account for orientations of individual plies and to model the laminate and material behaviour precisely. The cylindrical bullet of mass 6.42 g and length 25.3 mm impacts the centre of the workpiece in the axial direction using a velocity boundary condition. The details are listed in Table 4.

![Figure 2. FE model: bullet impact on plain weave E-glass fabric/epoxy composite plate](image)

A parametric study was carried out to analyse the effect of plate thickness on the ballistic limit velocity. The mechanical properties and geometry of the bullet used earlier simulations were kept constant. The plate thicknesses studied are listed in Table 4.

4.5 Finite elements and mesh sensitivity
In the rectangular composite plate, each ply was discretised with eight-node linear brick elements with reduced integration (C3D8R) through its thickness in the vicinity of the impact zone. Mesh-sensitivity analysis is important in simulations involving high deformations with a non-linear material response. Thus, a rigorous mesh-sensitivity study was carried out to obtain a computationally accurate finite-element mesh. In the current study all results are presented based on simulations performed with an optimised mesh. A computing clock time was reduced by introducing different mesh sizes in distinct regions of the FE model. A planar mesh size of 0.25 mm × 0.25 mm (in 1–2 plane) in the vicinity of
the impacted area was used; while a coarser mesh of 1.25 mm × 1.25 mm was employed in the area away from the zone of interest.

**Table 4. Loading and boundary conditions**

| Material studied | Plain-weave E-glass fabric composite (symmetric balanced cross-ply), thickness : 3mm |
|------------------|-------------------------------------------------------------------------------------|
| Parametric study | Plate thickness : 2.5 mm, 4.5 mm and 5 mm                                            |
| Impactor         | Cylindrical bullet (mass : 6.42 g, length : 25.3 mm)                                 |
| Impactor velocity, \(V_{50}\) (m/s) | 98 m/s                                                                            |
| Boundary condition | All four edges of plate fixed                                                          |

Localised stiffness reduction due to internal damage can cause excessive element distortion that could lead to difficulties in numerical convergence. To resolve this numerical issue, ‘distortion control’ was used in ABAQUS, and the damage variables were limited to a maximum value of 0.999. At each ply interface, cohesive elements of type COH3D8 with a thickness of 10 µm were embedded and used to model delamination initiation and growth with the failure criterion discussed in Section 4.2. The degradation parameters were set to 0.99, and failed cohesive elements were removed from the FE model, once the failure criteria were satisfied.

It was observed in the wave-stability study that cohesive zone elements govern stability of the solution (due to near-zero element size) with a very low stable time increment of the order of 10⁻⁹ s which affected the overall solution run time and, hence, a selective mass-scaling technique was used [10]. The density of cohesive-zone elements was artificially increased by a factor of 25 so that the total mass of the laminate remained practically unaffected. The bullet was meshed with one-integration-point, hexahedral elements of type C3D8R.

The model consisted of 575000 finite elements and analysis was completed in two and half hours using eight parallel processors on Intel Core i7 CPU with 8 GB RAM.

5. Simulation results and discussion

The effect of increase in the thickness of composite plate on the ballistic limit velocity (\(V_{50}\)) is shown in figure 3 and the magnitudes of \(V_{50}\) are listed in Table 5.

**Table 5. Magnitude of ballistic limit velocity (\(V_{50}\)) at varying composite plate thickness**

| Thickness of composite plate (mm) | Ballistic limit velocity (\(V_{50}\)) |
|----------------------------------|-------------------------------------|
| 2.5                              | 73.5 FE Analysis, 72 Experiment      |
| 3                                | 98 FE Analysis, 98 Experiment        |
| 4.5                              | 118.5 FE Analysis, 118 Experiment    |
| 5                                | 130.5 FE Analysis, 130 Experiment    |

It can be observed from Table 5 that, the increase in plate thickness by 66% (considering \(t = 3\) mm as the base plate thickness) resulted in a higher ballistic limit increase by 33%, while decrease in plate thickness by 16% caused reduction in \(V_{50}\) by 25%.

It was found that the kinetic energy absorbed by the composite plate during impact did not increase linearly with increase in the plate thickness, thus the primary reason of this increase in the ballistic limit can be attributed to the prolonged contact. It can be safely assumed that at high loading rates, as normally observed in ballistics, the glass fabric undergoes considerable hardening, before failure – thus providing extended resistance.
6. Conclusions
The ballistic impact behaviour of plain-weave E-glass fabric/epoxy composite was studied experimentally and numerically. A ply-level 3D finite element model was developed to analyse the response of the plate of this composite impacted by a cylindrical bullet. This FE model was validated with the experimental results and later used to predict the ballistic limit velocity for varying plate thicknesses. A good correlation was obtained with the experimental data emphasising the effectiveness of this model to predict the mechanical response of woven fabric composites under high-velocity impact loading.

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Figure 3. Effect of composite plate thickness on ballistic limit velocity (V_{50})