Abstract

Numerical models are investigated and refined for analysis of ultra-high molecular weight polyethylene (UHMW-PE) composite under ballistic and hypervelocity impact. An existing non-linear orthotropic continuum model implemented in a commercial hydrocode (ANSYS® AUTODYN®) was evaluated using a previously published material data set. It was found that the material through-thickness shear performance was artificially degraded as a result of coupling to the through-thickness tensile properties, significantly affecting the model accuracy for impact velocities in the ballistic regime. In order to correct for this, the composite laminate was discretized into sub-laminates joined by bonded contacts breakable through a combined tensile and shear stress failure criterion. The sub-laminate method allows the through-thickness tension and shear failure to be decoupled in the bulk material. This method was investigated and validated against experimental ballistic and hypervelocity impact tests. Simulations showed improvement in ballistic limit predictions for thin targets under low velocity impact. Prediction of target deflection is also significantly improved compared to the baseline model in terms back face deformation. Under hypervelocity impact, good agreement with the experimental ballistic limit and residual velocities is still maintained, with a small variation between the new and baseline models. A third validation case was performed to investigate the model performance with thicker targets (50 mm) for impact velocities between the two baseline studies. Accuracy for this condition is poor and remains a challenge for the numerical model.

Key words: fibre reinforced composite; impact; penetration; numerical model, polyethylene

1. Introduction

Ultra-high molecular weight polyethylene (UHMW-PE) composite is a promising ballistic armour material due to its high specific strength and stiffness. The ballistic efficiency of UHMW-PE composite has been previously investigated for a wide range of panel thicknesses against fragment simulating projectiles (FSP) [1], and been shown to be more weight efficient than traditional aramid and metallic armour for this class of penetrator.
The numerical modelling of composite materials under impact can be performed at a constituent level (i.e. explicit modelling of fibre and matrix elements, e.g. [2]), a meso-mechanical level (i.e. consolidated plies or fibre bundles, e.g. [3]), or macro-mechanically in which the composite laminate is represented as a continuum. In [4–7] a non-linear orthotropic continuum material model was developed and implemented in a commercial hydrocode (ANSYS® AUTODYN®) for application with aramid- and carbon fibre composites under hypervelocity impact. The non-linear orthotropic material model includes orthotropic coupling of the material volumetric and deviatoric responses, a non-linear equation of state (EoS), orthotropic hardening, combined stress failure criteria and orthotropic energy-based softening. For more detail refer to [8]. Lässig et al. [9] conducted extensive experimental characterisation of Dyneema® HB26 UHMW-PE composite for application in the continuum non-linear orthotropic material model, and validated the derived material parameters through simulation of spherical projectile impacts at hypervelocity.

In this paper, the modelling approach and material model parameter set developed in [9] is evaluated and refined for the simulation of impact events from 400 m/s > V > 6600 m/s. Across this velocity range the sensitivity of the numerical output is driven by different aspects of the material model, e.g. the strength model in the ballistic regime and the equation of state (EoS) in the hypervelocity regime. Therefore, by considering such a wide range of impact conditions for evaluation, a robust evaluation of the validity of the numerical approach, material model, and material parameter set is provided.

2. Background

In [9], numerical simulations of 15 kg/m² Dyneema® HB26 panels impacted by 6 mm diameter aluminum spheres between 2052 m/s to 6591 m/s were shown to provide very good agreement with experimental measurements of the panel ballistic limit and residual velocities, see Fig. 1. The modelling approach and material parameter set from [9] were applied to simulate impact experiments at velocities in the ballistic regime (here considered as <1000 m/s). In Fig. 1 the results of modelling impact of 20 mm fragment simulating projectiles (FSPs) against 10 mm thick Dyneema® HB26 are shown. The model shows a significant under prediction of the ballistic limit (V50), 236 m/s compared to 394 m/s.

![Fig. 1. Experimental and numerical impact residual velocity results for impact of 6 mm diameter aluminium spheres against 15 kg/m² Dyneema® HB26 at normal incidence (left) and impact of 20 mm fragment simulating projectiles against 10 mm thick Dyneema® HB26 at normal incidence (right). Lambert-Jonas parameters (a, p, Vbl) are provided in the legend.](image)

3. Modifications to the existing modelling approach

Material failure in the non-linear orthotropic material model is determined by a modified Hashin-Tsai failure criterion, where failure in a particular direction is initiated when:
where $\sigma$ is the stress, $S$ is the failure stress, and $D$ is the damage. Damage acts in this model to reduce the effective strength of the material and is based on material fracture energy [8]. As per Eq.(1), when an element fails in one particular direction (i.e. 11), damage begins to accumulate in the other directions (i.e. 12 and 31). UHMW-PE composite has a very low through-thickness tensile strength ($S_{11}$) compared to the through-thickness shear strength ($S_{12}$ or $S_{32}$). As a result, through-thickness tensile failure strength can result in premature through-thickness shear failure, demonstrated in Fig. 2.

The response of UHME-PE composite under ballistic impact by blunt fragments was found in [1] to be heavily dependent on through-thickness shear behavior. Thus, premature through-thickness shear failure as depicted in Fig. 2, would clearly explain the under prediction of material performance in the ballistic condition plotted in Fig. 1. As armour performance at hypervelocity is less dependent on strength, the high level of agreement presented for the hypervelocity impact condition in Fig. 1 is not a contradiction.

In order to prevent premature through-thickness shear failure, the through-thickness tensile failure was decoupled from Eq. (1) by setting $S_{11} \to \infty$. Through-thickness tensile failure was maintained in the model by discretizing the laminate into arbitrary sub-laminates (i.e. multiple parts through the thickness) kinematically joined and breakable through a criterion that combines normal and shear stress,

$$\left(\frac{\sigma_N}{S_N}\right)^a + \left(\frac{\sigma_S}{S_S}\right)^b \geq 1$$

where $\sigma$ and $S$ are stress and strength values and subscripts $N$ and $S$ are normal and shear directions, respectively. Here $S_N$ is set to 1.07 MPa and $S_S$ is set to 2.61 MPa from mechanical test data in [9]. The exponents $a$ and $b$ were assumed to be 1 in the absence of combined loading data. With this method, through-thickness tensile failure occurs at the sub-laminate interfaces rather than in the bulk material.

Fig. 2. Left: The effect of varying through-thickness tensile strength on the shear stress-strain results from a single element simulation; Right: Free surface velocity trace from inverse flyer plate impact experiment and numerical simulation.
Simulations with the sub-laminate approach using the polynomial formulation of the Mie-Grüneisen EoS were observed to be unstable, typically producing highly distorted elements with extremely high internal energy and small time steps. The polynomial EoS allows for differing description of compression and expansion response. Under expansion, a minimum pressure exists that can leads to large increases in volume with minimal changes in pressure. However, since through-thickness tensile failure in the bulk material is disabled in the sub-laminate, the minimum pressure under expansion is less than the fracture strength of the material in through-thickness tension. Therefore, the strength and failure model suppresses fracture in tension in the bulk material while the EoS tries to expand the volume of material under negative pressure, leading to large distortion of elements and eventually numerical instability. Since the sub-laminate approach allows through-thickness failure at the sub-laminate interface, a different description of expansion is not critical. Thus the shock formulation of the Mie-Grüneisen EoS, which defines the same material response in compression and expansion, is used to describe the volumetric response of the bulk material. The shock EoS is described by a linear function of the shock-particle velocity relationship,

\[ U_s = c_0 + S U_p \]  (3)

Here the bulk sound speed \( c_{sp} \) is related to the effective bulk modulus of the material. The slope of the curve \( S \), is calibrated to match flyer plate impact tests in \cite{9} and the Grüneisen coefficient is set to 1.6, corresponding to the value reported by Meyers \cite{10} for polyethylene. Fig. 2 compares the free surface velocity, \( V_{fs} \), measured from inverse flyer plate impact tests (IFPT) in \cite{9} with one-dimensional inverse flyer plate impact simulations. Free surface velocity measurements in IFPT are considered one-dimensional as long as the stress waves propagating from the lateral edges have not reached the specimen centre. \( V_{fs} \), measurements are typically recorded using laser based methods such as VISAR (velocity interferometer system for any reflector). In these tests the flyer plate is a 50 mm diameter disk, thus the one-dimensional strain assumption holds true until the impact shock wave can travel from the outer edge of the flyer plate to the centre (i.e. half the diameter). The in-plane wave speed of UHMW-PE composite is approximated by:

\[ c = \sqrt{\frac{E_f v_f}{2 \rho_f}} \]  (4)

where \( E_f \) is the fibre elastic modulus (130 GPa), \( \rho_f \) is the fibre density (980 kg/m\(^3\)) and \( v_f \) is the composite fibre volume fraction (80%). A factor of 1/2 is included to account for the cross-ply layup of the material. Therefore the time required for the wave to propagate from the edge to the centre of the UHMW-PE composite specimen is approximately 3400 ns, and approximately 4150 ns for the steel witness plate. As shown in Fig. 2, the velocity jump relating to the initial and subsequent release waves are well matched with the one-dimensional inverse flyer plate impact simulation. Approximately 3000-3500 ns after impact the experimental and numerical results deviate as the experimental conditions can no longer be considered one-dimensional.

4. Results and discussion

3D numerical simulations were performed of the full target and projectile, where both were meshed using 8-node hexahedral elements. The projectile was meshed with 9 elements across the diameter. The target is composed of sub-laminates that are one element thick, separated by a small gap to satisfy the master-slave contact algorithm (external gap in AUTODYN®) and bonded together as previously discussed. The mesh size of the target is approximately equal to the projectile at the impact site. The mesh was then graded towards the edge, increasing in coarseness to reduce the computational load of the model. Since UHMW-PE composite has a very low coefficient of friction, force fit clamping provides little restraint. High speed video of ballistic impact tests typical showed clamp slippage upon impact. As such no boundary conditions were imposed on the target. The FSP material was modelled as Steel S-7 from the AUTODYN® library, described using a linear EoS and the Johnson-Cook strength model \cite{11}. The aluminium sphere was modelled using AL1100-O from the AUTODYN® library that uses a shock EoS and the Steinburg Guinan strength model \cite{12}. The master-slave contact algorithm was used to detect contact between the target and projectile.

The sub-laminate model with shock EoS was applied to the aluminium sphere hypervelocity impact series and 20 mm FSP ballistic impact series presented in Fig. 1, the results of which are shown in Fig. 3. The sub-laminate model is shown to provide a significant improvement in predicting the experimental \( V_{50} \) of 394 m/s for the FSP ballistic impacts (377 m/s) compared to the monolithic model (236 m/s). The ballistic limit and residual velocity predicted with the sub-laminate model for the
hypervelocity impact case are shown to be comparable with the original monolithic model. For conditions closer to the ballistic limit, the sub-laminate model is shown to predict increased target resistance (i.e. lower residual velocity). For higher overmatch conditions, e.g. \( \text{V}_i = 6591 \text{ m/s} \), there is some small variance between the two approaches.

![Figure 3](image)

**Fig. 3.** Comparison of the experimental results with the two numerical models for impact of 20 mm fragment simulating projectiles against 10 mm thick Dyneema HB26\(^a\) at normal incidence (left), and impact of 6 mm diameter aluminium spheres against 15 kg/m\(^2\) Dyneema\(^a\) HB26 at normal incidence (right). Lambert-Jonas parameters (a, p, V\(_{\text{bl}}\)) are provided in the legend.

![Figure 4](image)

**Fig. 4.** Bulge of a 10 mm target impact by a 20 mm FSP at 365 m/s (experiment) and 350 m/s (simulations), 400 \( \mu \text{s} \) after the initial impact.

In Fig. 4 a qualitative assessment of the bulge formation is made for the 10 mm panel impacted at 365 m/s (i.e. below the \( \text{V}_{50} \)) by a 20 mm FSP. Prediction of bulge development is important as it is characteristic of the material wave speed and is also a key measure in defence applications, particularly in personnel protection (i.e. vests and helmets). The sub-laminate model is shown to accurately reproduce the characteristic pyramid bulge shape and drawing of material from the lateral edge. In comparison, the bulge prediction of the baseline model is poor, showing a conical shape with the projectile significantly behind the apex. In the baseline model penetration occurs through premature through-thickness shear failure around the projectile rather than in-plane tension (membrane) which would allow the formation of a pyramidal bulge as the composite is carried along with the projectile. Furthermore, in the baseline model the extremely small through thickness tensile strength (1.07 MPa) in the bulk
material leads to early spallation/delamination of the back face. This allows the material on the target back face to fail and be accelerated ahead of the projectile. In the sub-laminate model, these two artifacts are addressed, and so a more representative bulge is formed.

5. Further experimental validation

A third experimental condition was considered to validate the numerical approach for a velocity between the two conditions previously discussed, namely the ballistic limit of a 50 mm thick Dyneema® HB26 impacted by 12.7 mm FSPs. In [1] the experimental $V_{50}$ was reported as 1657 m/s. Using the sub-laminate approach with shock EoS, the numerical model predicted a $V_{50}$ of 944 m/s. The results of the simulation are compared to the experimental measurement in Fig. 5 ($V_{50}$ only, no residual velocity measurements were made).

The significant decrease in accuracy of the numerical prediction may be due to an insufficient in-plane tensile strength defined in the material data set (Appendix A), a critical material property in the ballistic performance of fibre reinforced composites [13]. Numerous researchers have investigated the tension properties of UHMW-PE composite (e.g. [14,15]). These tests showed that due the low friction coefficient and poor fibre/matrix adhesion in UHMW-PE composites, specimen gripping is problematic, often leading to delamination rather than tension failure [3]. Chocron et al. [3] used fibre properties to successfully model UHMW-PE composite under impact using a meso-scale model. This suggests that the in-plane tensile properties of the continuum laminate should also be in reasonable agreement to the fibre properties based on the rule of mixtures (ignoring strength contribution from the matrix). With a fibre tensile strength of 3.5 GPa, fibre volume fraction of 0.80 and accounting for the cross ply layup, the in-plane tensile strength according to the rule of mixture is approximately 1400 MPa. In this work, an in-plane tensile strength of 751 MPa was used from experimental measurements of a through-bolted composite specimen [9]. In these tests, the test section is not directly clamped and so the transfer of load from the outer plies to the inner plies occurs through interlaminar shear. Since the interlaminar shear properties of UHMW-PE composite is very low, effective transmission of load through the thickness of the specimen cannot occur and so may fail by delamination. This may lead to strength not representative of the materials true tensile strength.

In the case of a 10 mm thick target impacted by a 20 mm FSP, the $V_{50}$ was under-predicted by 4%. For the 50 mm thick target impacted by a 12.7 mm FSP, the $V_{50}$ is under-predicted by 43%. Since the target is significantly thicker, the error associated with the lower in-plane strength becomes increasingly pronounced. Fig. 5 shows the comparison of the deformation
near the failure threshold. Due to the low tensile strength, large membrane stresses cannot be maintained and as a result, the bulge is smaller than would be expected for a target close to the ballistic limit.

6. Conclusions

The non-linear orthotropic continuum model proposed in [9] was evaluated for UHMW-PE composite under impact by blunt fragments across a wide range of impact velocities. Although previously found to provide accurate results for hypervelocity impact of aluminium spheres, the existing model and dataset was found to significantly underestimate the composite performance under impact conditions driven by through-thickness shear performance (ballistic impact of fragmental projectile). The model was found to exhibit premature through-thickness shear failure as a result of directional coupling in the modified Hashin-Tsai failure criterion and the large discrepancy between through-thickness tensile and shear strength of UHME-PE composite. As a result, premature damage and failure was initiated in the through-thickness shear direction leading to decreased ballistic performance. By de-coupling through-thickness tensile failure from the failure criteria and discretizing the laminate into a nominal number of kinematically joined sub-laminates through the thickness, improvements in modelling the ballistic response of the panels was improved. Additionally, instabilities introduced into the model as an artifact of disabling through-thickness tensile failure in the bulk material were overcome by application of a shock formulation of the Mie-Grüneisen EoS rather than the polynomial formulation. The sub-laminate model with shock EoS showed improved qualitative and quantitative predictions of thin targets under ballistic impact conditions (10 mm against 20 mm FSP) compared to the baseline model in [9], with comparative predictive accuracy for thin targets under hypervelocity impact conditions (15 kg/m² against Al1100-O spheres). A third validation case was considered for impact velocities between the ballistic and hypervelocity cases, with the additional complexity of a significantly thicker target panel (50 mm against 12.7 mm FSP). For this condition the ballistic limit was significantly under predicted. Evaluation of the result suggests that the failure mechanisms which drive performance in the rear section of the target panel (i.e. membrane tension) were not adequately reproduced, suggesting an under-estimate of the material in-plane tensile performance. Although based on experimental measurements, the in-plane tensile behavior of UHMW-PE composite is difficult to accurately measure due to difficulties in gripping the specimen and a tendency for the material to delaminate or creep.

Acknowledgements

The work reported herein was funded by the Defence Science and Technology Organization (DSTO) under the Land Vehicle Survivability Science and Technology Capability (STC). The authors would also like to acknowledge the support of RMIT University and the Defence Materials Technology Centre (DMTC), which was established and is supported by the Australian Government’s Defence Future Capability Technology Centre (DFCTC) initiative, who provide a joint PhD scholarship for the lead author.

References

[1] Nguyen L.H., Ryan S., Cimpoeru S.J., Mouritz A.P., Orifici A.C., 2015. The effect of target thickness on the ballistic performance of ultra high molecular weight polyethylene composite, International Journal of Impact Engineering 75, p. 174–83.
[2] Segala D.B., Cavallaro P.V., 2014. Numerical investigation of energy absorption mechanisms in unidirectional composites subjected to dynamic loading events, Computational Materials Science 81, p.303–12.
[3] Chocron S., Nicholls A.E., Brill A., Malka A., Namir T., Havazelet D., Van Der Werf H., Heisserer U., Walker J.D., 2014. Modeling unidirectional composites by bundling fibers into strips with experimental determination of shear and compression properties at high pressures, Composites Science and Technology 101, p. 32–40.
[4] Hayhurst C.J., Hiermaier S.J., Clegg R.A., Riedel W., Lambert M., 1999. Development of material models for nextel and kevlar-epoxy for high pressures and strain rates, International Journal of Impact Engineering 23, p.365–376.
[5] Clegg R.A., White D.M., Riedel W., Harwick W., 2006. Hypervelocity impact damage prediction in composites: Part I—material model and characterisation, International Journal of Impact Engineering 33, p. 190–200.
[6] Riedel W., Nahme H., White D.M., Clegg R.A., 2006. Hypervelocity impact damage prediction in composites: Part II—experimental investigations and simulations, International Journal of Impact Engineering 33, p. 670–80.
[7] Wicklein M., Ryan S., White D.M., Clegg R.A., 2008. Hypervelocity impact on CFRP: Testing, material modelling, and numerical simulation, International Journal of Impact Engineering 35, p.1861–1869.
Table 1. Non-linear orthotropic continuum model dataset for UHMW-PE composite in ANSYS® AUTODYN® [9], direction 11 denotes through-thickness.

| EOS: Ortho | Strength: Orthotropic Yield |
|------------|-----------------------------|
| Parameter  | Symbol | Value  | Units | Parameter      | Symbol | Value  | Units |
| Reference Density | $\rho$ | 0.98 | g/cm$^3$ | Plasticity constant 11 | $A_{11}$ | 0.03 | - |
| Young's Modulus 11 | $E_{11}$ | 3.62×10$^4$ | kPa | Plasticity constant 22 | $A_{22}$ | 1×10$^5$ | - |
| Young's Modulus 22 | $E_{22}$ | 2.69×10$^4$ | kPa | Plasticity constant 33 | $A_{33}$ | 1×10$^5$ | - |
| Young's Modulus 33 | $E_{33}$ | 2.60×10$^4$ | kPa | Plasticity constant 12 | $A_{12}$ | 1×10$^6$ | - |
| Poisson's Ratio 12 | $\nu_{12}$ | 0.013 | - | Plasticity constant 13 | $A_{13}$ | 1×10$^6$ | - |
| Poisson's Ratio 23 | $\nu_{23}$ | 0 | - | Plasticity constant 23 | $A_{23}$ | 1×10$^6$ | - |
| Poisson's Ratio 31 | $\nu_{31}$ | 0.5 | - | Plasticity constant 44 | $A_{44}$ | 1 | - |
| Shear Modulus 12 | $G_{12}$ | 3.07×10$^3$ | kPa | Plasticity constant 55 | $A_{55}$ | 1.75 | - |
| Shear Modulus 23 | $G_{23}$ | 4.23×10$^3$ | kPa | Plasticity constant 66 | $A_{66}$ | 1.75 | - |
| Shear Modulus 31 | $G_{31}$ | 3.07×10$^3$ | kPa | Eff. Stress #1 | $\sigma_{e1}$ | 176 | kPa |
| Volumetric Response: Shock | Parameter | Symbol | Value  | Units | Eff. Stress #2 | $\sigma_{e2}$ | 989 | kPa |
| Grüneisen coefficient | $\Gamma$ | 1.6 | - | Eff. Stress #3 | $\sigma_{e3}$ | 1.74×10$^4$ | kPa |
| Parameter C1 | $c_0$ | 2.68×10$^4$ | m/s | Eff. Stress #4 | $\sigma_{e4}$ | 2.42×10$^4$ | kPa |
| Parameter S1 | $s$ | 1.8 | - | Eff. Stress #5 | $\sigma_{e5}$ | 3.1×10$^3$ | kPa |
| Reference Temperature | $T_0$ | 293 | k | Eff. Stress #6 | $\sigma_{e6}$ | 5.97×10$^3$ | kPa |
| Specific Heat | $c$ | 1.85×10$^7$ | J/kgK | Eff. Stress #7 | $\sigma_{e7}$ | 1.2×10$^4$ | kPa |
| Failure: Orthotropic Damage | Parameter | Symbol | Value  | Units | Eff. Stress #8 | $\sigma_{e8}$ | 2.07×10$^4$ | kPa |
| Tensile Failure Stress 11 | $S_{11}$ | 1.01×10$^{10}$ | kPa | Eff. Stress #9 | $\sigma_{e9}$ | 3.46×10$^3$ | kPa |
| Tensile Failure Stress 22 | $S_{22}$ | 7.53×10$^9$ | kPa | Eff. Stress #10 | $\sigma_{e10}$ | 2.02×10$^3$ | kPa |
| Tensile Failure Stress 33 | $S_{33}$ | 7.53×10$^9$ | kPa | Eff. Plastic Strain #1 | $\varepsilon_{e1}$ | 1.82×10$^{-6}$ | - |
| Maximum Shear Stress 12 | $S_{12}$ | 1.01×10$^{10}$ | kPa | Eff. Plastic Strain #2 | $\varepsilon_{e2}$ | 0.0012 | - |
| Maximum Shear Stress 23 | $S_{23}$ | 3.52×10$^9$ | kPa | Eff. Plastic Strain #3 | $\varepsilon_{e3}$ | 0.00311 | - |
| Maximum Shear Stress 31 | $S_{31}$ | 1.01×10$^{10}$ | kPa | Eff. Plastic Strain #4 | $\varepsilon_{e4}$ | 0.00692 | - |
| Fracture Energy 11 | $G_{11C}$ | 790 | J/m$^2$ | Eff. Plastic Strain #5 | $\varepsilon_{e5}$ | 0.0113 | - |
| Fracture Energy 22 | $G_{22C}$ | 30 | J/m$^2$ | Eff. Plastic Strain #6 | $\varepsilon_{e6}$ | 0.0283 | - |
| Fracture Energy 33 | $G_{33C}$ | 30 | J/m$^2$ | Eff. Plastic Strain #7 | $\varepsilon_{e7}$ | 0.0578 | - |
| Fracture Energy 12* | $G_{12C}$ | 1.46×10$^3$ | J/m$^2$ | Eff. Plastic Strain #8 | $\varepsilon_{e8}$ | 0.106 | - |
| Fracture Energy 23* | $G_{23C}$ | 1.46×10$^3$ | J/m$^2$ | Eff. Plastic Strain #9 | $\varepsilon_{e9}$ | 0.1061 | - |
| Fracture Energy 31* | $G_{31C}$ | 1.46×10$^3$ | J/m$^2$ | Eff. Plastic Strain #10 | $\varepsilon_{e10}$ | 1 | - |
| Damage Coupling Coefficient | C | 0.5 | - | Erosion: Instantaneous Geometric Strain |

* Kevlar Epoxy from [5]