The Ballistic Performance of Thick Ultra High Molecular Weight Polyethylene Composite

A thesis submitted in fulfilment of the requirements for the degree of Doctor of Philosophy

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Declaration

I certify that except where due acknowledgement has been made, the work is that of the author alone; the work has not been submitted previously, in whole or in part, to qualify for any other academic award; the content of the thesis/project is the result of work which has been carried out since the official commencement date of the approved research program; any editorial work, paid or unpaid, carried out by a third party is acknowledged; and, ethics procedures and guidelines have been followed.

Long Nguyen
December 2015
Abstract

Ultra-high molecular weight polyethylene (UHMW-PE) fibre-reinforced composite is a promising material for ballistic protection due to its high strength, stiffness and low density. The use of UHMW-PE composite as part of the primary armour system in vehicles has the potential to provide significant weight savings or improved protection levels over traditional metallic materials. Although already used in vehicle armours, both as spall liners and within complex multi-element/multi-material packages, there is a limited understanding of the mechanisms driving ballistic performance. Existing analysis tools do not allow for a good approximation of performance, while existing numerical models are either incapable of accurately capturing the response of thick UHMW-PE composite to ballistic impact or are unsuited to model thick targets. In response, this thesis aims to identify the key penetration and failure mechanisms of thick UHMW-PE composite under ballistic impact and develop analytical and numerical models that capture these mechanisms and allow accurate prediction of ballistic performance.

The response of thick UHMW-PE composite to ballistic impact was experimentally investigated. Panel thicknesses ranging from 9 mm to 100 mm were investigated, which is about 3 times thicker than what has previously been reported in the literature. The targets were impacted by 12.7 mm and 20 mm calibre fragment simulating projectiles (FSPs), which are standardised surrogates of artillery round fragments, to determine the ballistic limit velocity ($V_{50}$). The penetration and failure mechanisms were identified by inspection of impacted targets, and scanning electron microscopy was conducted to inspect the load-bearing fibres around the penetration cavity. Thick targets demonstrated a two-stage penetration process: shear plugging during the initial penetration followed by the formation of a transition plane and bulging of a separated rear panel. The transition between the two penetration stages is an important characteristic of thick UHMW-PE composite but is currently poorly understood. In this work, transition was characterised for a wide range of thickness and impact velocities and the mechanisms for transition were identified.

This work also evaluates the ballistic performance of UHMW-PE composite against a range of common metallic and composite armour materials. For armour design, assessment of the mass and space efficiency is important in gauging the quality and suitability of an armour material. For UHMW-PE, no such studies have been previously published. Additional ballistic limit tests were performed on two different armour steels which served as a reference material for the efficiency evaluation. The mass and space efficiency of UHMW-PE composite against FSPs was compared to rolled homogeneous armour steel (RHA), high hardness armour steel (HHA), an aluminium alloy, and polymer composites reinforced with continuous aramid, glass or carbon fibres. UHMW-PE composite is shown to be 300% to 500% more mass efficient than the metallic armour materials against FSPs and 130% to 160% more mass efficient than the fibre-reinforced composites considered. UHMW-PE composite achieves a higher mass efficiency compared to these other armour materials because the penetration mechanism it exhibits (shear plugging and bulging) are more effective in resisting penetration than the adiabatic shear plugging mechanisms.
in metals and compression loading in glass and carbon fibre-reinforced composites. The response of aramid fibre-reinforced composite to ballistic impact is similar to UHMW-PE composite, however the tensile properties of aramid fibre are lower than UHMW-PE fibre, giving rise to lower ballistic performance.

A new analytical model was developed for thick UHMW-PE composite impacted by blunt projectiles to describe the two stages of penetration identified in the experimental work. The analytical model is based on energy and momentum conservation laws, and uses an energy balance between the projectile kinetic energy and the energy absorbed by the target to predict the ballistic limit velocity. The energy absorbed during the initial shear plugging stage was analytically described in terms of work required to produce a shear plug in the target material, while the bulging phase was based on momentum conservation and classical yarn theory. The model was validated against the ballistic limit results determined from the experimental program. Existing analytical models based on membrane theory were validated against results of thinner targets reported in literature and demonstrated to be suitable. Excellent agreement was shown between the analytical model and experimental results. The analytical approach allows the prediction of ballistic performance of UHMW-PE composite for thick and thin targets against blunt projectiles.

A numerical modelling methodology was developed for the ballistic impact analysis of thick UHMW-PE composite using a commercial hydrocode. The methodology uses a continuum model that captures non-linear shock compressibility of the material, orthotropic elastic-plastic strength, and orthotropic failure criteria. A novel approach was used to model interlaminar failure by dividing the panel into sub-laminates connected by breakable bonds. A new failure-based element erosion/deletion model was implemented with a user subroutine, which more accurately accounts for the directional properties of fibre-reinforced composites than existing strain-based models. The model is extensively validated against experimental ballistic data. The model gave excellent predictions for depth of penetration, within 5% of experiment, and good predictions of ballistic limit and residual velocity, within 20% of experiment, for all conditions considered (12.7 mm and 20 mm FSP, 9 mm to 100 mm thick targets and impact velocities between 400 m/s to 2000 m/s). The predictions of the penetration mechanisms and target bulge behaviour were also compared in terms of the target shear plugging ratio and bulge hinge and apex position, also demonstrating very good correlation with the experimental results. The numerical model was also used to further investigate the ballistic impact response of UHMW-PE composite, including the mechanisms underpinning transition from shear plugging to bulging in thick targets and the effect of fibre bridging and thermal softening on the ballistic performance.

The research outcomes from this PhD thesis have made original contributions to our understanding of thick UHMW-PE composite under ballistic impact loading. The key penetration and failure mechanisms of thick UHMW-PE composite under ballistic impact were identified, and the transition between different penetration mechanisms was characterised. A new analytical model based on these observations was developed that allows ballistic limit predictions of thick UHMW-PE composite against blunt projectiles. A numerical modelling methodology was developed that allows efficient and accurate simulation of thick UHMW-PE composite under ballistic impact. The knowledge and analysis tools developed in this thesis will promote and aid in the development of improved armour systems incorporating UHMW-PE composite in the future.
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# Nomenclature

## Acronyms

<table>
<thead>
<tr>
<th>Acronym</th>
<th>Description</th>
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<tbody>
<tr>
<td>AFRP</td>
<td>Aramid fibre-reinforced plastic</td>
</tr>
<tr>
<td>CFRP</td>
<td>Carbon fibre-reinforced plastic</td>
</tr>
<tr>
<td>DoP</td>
<td>Depth of penetration</td>
</tr>
<tr>
<td>EoS</td>
<td>Equation of state</td>
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<tr>
<td>FSP</td>
<td>Fragment simulating projectile</td>
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<tr>
<td>GFRP</td>
<td>Glass fibre-reinforced plastic</td>
</tr>
<tr>
<td>HHA</td>
<td>High hardness armour steel</td>
</tr>
<tr>
<td>HVI</td>
<td>Hypervelocity</td>
</tr>
<tr>
<td>IFPI</td>
<td>Inverse flyer plate impact</td>
</tr>
<tr>
<td>IGS</td>
<td>Instantaneous geometric strain</td>
</tr>
<tr>
<td>RHA</td>
<td>Rolled homogeneous armour steel</td>
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<tr>
<td>SEM</td>
<td>Scanning electron microscopy</td>
</tr>
<tr>
<td>SHPB</td>
<td>Split hopkinson pressure bar</td>
</tr>
<tr>
<td>SPH</td>
<td>Smoothed particle hydrodynamics</td>
</tr>
<tr>
<td>UHMW-PE</td>
<td>Ultra-high molecular weight polyethylene</td>
</tr>
<tr>
<td>VISAR</td>
<td>Velocity interferometer system for any reflector</td>
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## Symbols

<table>
<thead>
<tr>
<th>Symbol</th>
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<tbody>
<tr>
<td>$\beta$</td>
<td>Momentum area multiplier</td>
</tr>
<tr>
<td>$\Gamma$</td>
<td>Gruneisen coefficient</td>
</tr>
<tr>
<td>$\rho$</td>
<td>Density</td>
</tr>
<tr>
<td>$\sigma$</td>
<td>Stress</td>
</tr>
<tr>
<td>$\varepsilon$</td>
<td>Strain</td>
</tr>
<tr>
<td>$A$</td>
<td>Presented area</td>
</tr>
<tr>
<td>$AD$</td>
<td>Areal density</td>
</tr>
</tbody>
</table>
\begin{itemize}
  \item \(c\) \hspace{1cm} \text{Wave speed}
  \item \(D\) \hspace{1cm} \text{Damage}
  \item \(E\) \hspace{1cm} \text{Elastic modulus}
  \item \(e\) \hspace{1cm} \text{Internal energy}
  \item \(E_B\) \hspace{1cm} \text{Bulging energy}
  \item \(E_m\) \hspace{1cm} \text{Mass efficiency}
  \item \(E_S\) \hspace{1cm} \text{Shear plugging energy}
  \item \(E_s\) \hspace{1cm} \text{Space efficency}
  \item \(G_f\) \hspace{1cm} \text{Fracture energy}
  \item \(k\) \hspace{1cm} \text{Shear plugging ratio}
  \item \(m\) \hspace{1cm} \text{Mass}
  \item \(P\) \hspace{1cm} \text{Pressure}
  \item \(r\) \hspace{1cm} \text{Radius}
  \item \(S\) \hspace{1cm} \text{Strength}
  \item \(t\) \hspace{1cm} \text{Thickness}
  \item \(u_{fs}\) \hspace{1cm} \text{Free surface velocity}
  \item \(u_p\) \hspace{1cm} \text{Particle velocity}
  \item \(U_s\) \hspace{1cm} \text{Shock velocity}
  \item \(V\) \hspace{1cm} \text{Velocity}
  \item \(v\) \hspace{1cm} \text{Volume}
\end{itemize}

**Subscripts**

\begin{itemize}
  \item 1, 2, 3 \hspace{1cm} \text{Principal direction}
  \item 11, 22, 33 \hspace{1cm} \text{Material direction}
  \item 50 \hspace{1cm} \text{50th percentile}
  \item \(B\) \hspace{1cm} \text{Bulging}
  \item \(BL\) \hspace{1cm} \text{Ballistic limit}
  \item \(eff\) \hspace{1cm} \text{Effective}
  \item \(f\) \hspace{1cm} \text{Fibre}
  \item \(H\) \hspace{1cm} \text{Hugoniot}
  \item \(I\) \hspace{1cm} \text{Impact}
  \item \(max\) \hspace{1cm} \text{Failure}
\end{itemize}
<table>
<thead>
<tr>
<th>Symbol</th>
<th>Description</th>
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<tbody>
<tr>
<td>$p$</td>
<td>Projectile</td>
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<tr>
<td>$R$</td>
<td>Residual</td>
</tr>
<tr>
<td>$ref$</td>
<td>Reference</td>
</tr>
<tr>
<td>$S$</td>
<td>Shear plugging</td>
</tr>
<tr>
<td>$t$</td>
<td>Target</td>
</tr>
<tr>
<td>$vol$</td>
<td>Volumetric</td>
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Chapter 1

Introduction

Ultra-high molecular weight polyethylene (UHMW-PE) is a thermoplastic characterised by an extremely long molecular chain. Through a gel spinning process, the molecular chains are aligned and fibres are produced with very high specific strength and stiffness. These high strength fibres are particularly promising for ballistic protection having demonstrated very high penetration resistance for a given areal density. As a result, the fibres are increasingly being used to produce flexible fabrics for ballistic vests or as the reinforcement phase in more rigid fibre-reinforced composites to produce vest inserts or helmets. UHMW-PE composite is also typically used as a contact spall liner in vehicles to protect occupants from internal fragmentation from the ballistic threat or fragmentation of the primary armour of the vehicle. As such UHMW-PE composite is becoming an increasingly important part of modern armour systems as there is a significant need for lighter armour solutions for protection against existing and emerging ballistic threats.

The application of UHMW-PE composite today is predominantly in thin sections. This is because of the weight critical nature of the application (personal protection) or because UHMW-PE composite acts as a secondary component in an armour system (spall liner). However, thicker sections may be suitable for vehicle application as a primary armour component for protection against high lethality ballistic and fragmentation threats. Although the armour system is critical to the occupant’s survivability under blast and ballistic attack, the armour system is a dead weight to the vehicle in most of its operational life and has significant impact on mobility. As such, minimisation of the armour weight by utilising more ballistically effective but lightweight materials such as UHMW-PE composite is important to the operational effectiveness of the armoured vehicle.

The effective use of UHMW-PE composite for armour application can only be achieved when supported by an understanding of the material behaviour under impact loading. However, the response of many materials including metallic, non-metallic and fibre-reinforced composites to impact loading is different at different thickness scales. For example metals are known to exhibit several different penetration mechanisms including ductile hole formation, dishing, discing, shear plugging etc. for different target thicknesses (Woodward and Cimpoeur, 1998). Because of the extensive use of UHMW-PE composite in vests, helmets and as a spall liner, the response of thin UHMW-PE composite to ballistic impact has been extensively researched. For thicker sections of UHMW-PE composite however, there is limited understanding of the penetration and failure mechanisms of the material under ballistic impact. There is also limited published experimental data that would allow assessment of ballistic performance with increasing target thickness, while the deformation response of thick targets is also poorly understood. Characterisation and
understanding of the response of thick UHMW-PE composite to ballistic impact is therefore critical if the material is to be effectively used as a component of an applique (add-on) or primary armour system.

Since UHMW-PE composite can have variations in fibre grade, composite composition (fibre-resin content, woven or uni-directional and stacking sequence), processing conditions (temperature, pressure and time) and laminate thickness, experimental characterisation against the many ballistic threats is prohibitively expensive. Analysis tools such as analytical and numerical models that are able to predict the response and performance of the material are therefore essential to contain cost for the design, analysis and optimisation of armour systems incorporating UHMW-PE composite. Analytical and numerical models are also valuable as they can provide significant insights into the response of the material to impact loading. Analytical models can describe the key penetration and failure mechanisms, while numerical models allow investigation and measurement of important parameters that are often difficult or near impossible to measure experimentally. However, there is currently no analytical model that can predict the ballistic performance and describes the penetration and failure mechanisms of thick UHMW-PE composite under ballistic impact. Furthermore no numerical modelling strategy has been demonstrated that can successfully predict the ballistic performance and deformation of thick UHMW-PE composite. These capability gaps impose significant costs to the design of armour systems incorporating UHMW-PE composite as optimisation and verification is experimentally driven.

This PhD thesis aims to address some of these issues and improve on the state-of-the-art by:

1. developing a better scientific understanding of the ballistic impact response of thick UHMW-PE composite,
2. developing and validating an analytical model that predicts the ballistic performance and describes the penetration and failure mechanisms of thick UHMW-PE composite, and
3. developing and validating a numerical analysis methodology for thick UHMW-PE composite.

1.1 Thesis Outline

A comprehensive literature review is presented in Chapter 2 that covers the relevant aspects of UHMW-PE composite and its ballistic properties. An overview is provided on the material and its construction followed by a review of the literature on its ballistic properties. Analytical models developed for fabrics and composites are summarised, a benchmarking exercise is conducted with suitable models applied to UHMW-PE composite to evaluate their accuracy and provide grounds for further improvements. The literature concerning numerical modelling and analysis of composites is also summarised with a focus on UHMW-PE composite. Following this, a numerical modelling strategy was selected and benchmarked to investigate its capabilities to capture the key penetration mechanisms as well as its accuracy in predicting the ballistic performance of thick UHMW-PE composite. The results set a framework for research work by identifying critical gaps in the current state-of-the-art.

In Chapter 3, the performance and response of thick UHMW-PE composite to ballistic impact is experimentally characterised. Ballistic limit testing is performed for targets up to 100 mm thick against fragment simulating projectiles (FSPs). The ballistic limit velocities are reported and
analysis of the post-impacted targets is performed to identify the key penetration and failure mechanisms. Analysis is performed using scanning electron microscopy of the fractured fibres in the penetration cavity and visual inspection of the targets.

A new analytical model for predicting the ballistic limit of thick UHMW-PE composite is presented in Chapter 4, and is based on the penetration and failure mechanisms identified in Chapter 3. The model addresses deficiencies with existing models that were identified in the benchmark study in the literature review. The model is derived from conservation of energy and momentum, and accounts for the specific behaviour observed in experiments. Validation of the analytical model is performed by comparing the predicted ballistic limit velocity results against the experimental results reported in Chapter 3 and the literature.

In Chapter 5, a numerical modelling methodology is presented for the analysis of thick UHMW-PE composite under ballistic impact. This work is motivated by the deficiencies in the existing modelling approaches identified in the evaluation study performed in the literature review. The methodology uses an existing continuum model implemented in a commercial hydrocode that captures non-linear shock compressibility, orthotropic elastic-plastic strength, and orthotropic failure. In this work, deficiencies in the existing model are addressed in order to capture the ballistic impact response of UHMW-PE composite. A sub-laminate approach to discretise the target is developed to overcome weaknesses in the failure model and a new damage-based erosion model is implemented which is more suitable for anisotropic materials. The model is extensively validated against depth of penetration, ballistic limit and residual velocity results from experiments conducted in Chapter 3. The target bulging is also quantitatively compared with experiments to evaluate the accuracy of the model in predicting the target deformation response. Finally in this chapter, the numerical model is used to further investigate the ballistic impact behaviour of UHMW-PE composite. The effect of fibre bridging and thermal softening on ballistic performance is investigated and further insight is gain in the transition in penetration mechanism and delamination behaviour of the material under impact.

Finally, conclusions to this work are given in Chapter 6. A summary of the key findings are presented and the significance of the work is discussed. Following from this, thoughts for further working arising from this research program are provided.

### 1.2 List of Publication

This work has led to a number of publications including four peer-reviewed journal papers (three of which as lead author), two international peer-reviewed conference papers and two DMTC milestone reports.

**Journal Papers**


**Conference Papers**


**Reports**


Chapter 2

Literature Review

A comprehensive review is presented in this chapter of the current state-of-the-art of UHMW-PE composite under ballistic impact. The chapter begins with an overview of UHMW-PE composite in the context of its chemical composition and the manufacturing processes involved in producing UHMW-PE into a high strength material that can be used for ballistic applications. A review of research efforts dedicated to understanding the ballistic properties of UHMW-PE composites is then presented along with the penetration and energy absorption mechanisms that have been identified for UHMW-PE composite under ballistic impact. A distinction is made between the penetration mechanisms of thin and thick panels. This section is followed by a review of analytical models that describe the penetration of fibre-reinforced composites. Analytical models provide a fundamental insight into the key penetration mechanisms and are critical for a more complete understanding of the penetration event. The key mechanical properties of the material relevant to its penetration resistance are then reviewed. The material response to shock loading is also covered, which provides further insight into the material response under very high pressure and high strain rate conditions. A review of the current state of the art in numerical modelling of UHMW-PE composite under ballistic impact is presented. This is followed by a benchmarking exercise using the current state-of-the-art material model, which evaluates its ability to capture the key penetration mechanisms and predict ballistic performance. The final section of this literature review summarises the key findings in the literature and gaps that currently exist in our understanding of the ballistic properties of UHMW-PE composite.

2.1 Ultra-High Molecular Weight Polyethylene

UHMW-PE is a thermoplastic polymer made from very long molecular chains of polyethylene. Figure 2.1 shows the chemical structure of polyethylene, where in UHMW-PE the number of repeated chains \( n \) is in the order of \( 10^5 \), giving rise to molecular weights in the order of \( 10^6 \) (Van Dingenen, 2001).

\[
\begin{array}{c}
\vdots \\
C-C-C \\
\vdots \\
H-H/n
\end{array}
\]

Figure 2.1: Polyethylene
As a non-polar molecule, interaction between polyethylene molecules is by very weak Van der Waals forces. However, due to the ultra-long polymer chain, significant strength can be derived through a gel spinning process that produces highly oriented and crystalline molecular structures aligned in the spinning direction. The gel spinning process firstly involves dissolving UHMW-PE in a solvent at high temperature. The solution is then pushed through a spinneret to form liquid filament that is then quenched in water to form gel-fibres. These fibres are then drawn in hot air at high strain rates of the order of 1 s\(^{-1}\) forming fibres with smooth circular cross-sections approximately 17 µm in diameter (Russell et al., 2013) with a molecular orientation greater than 95% and a crystallinity of up to 85% (Van Dingenen, 2001), Figure 2.2.

These fibres are composed of smaller macro-fibrils approximately 0.5 µm to 2 µm in diameter, which in turn are made of micro-fibrils, 20 nm in diameter (Berger et al., 2003). Commercial UHMW-PE fibre is manufactured by, amongst others, DSM Dyneema and Honeywell under the trade names Dyneema® and Spectra®, respectively.

The fibres are used in a variety of applications requiring high specific strength and low weight. This includes high strength ropes and nets, cut-resistant gloves as well as blast and ballistic protection. For ballistic protection applications the fibres can be woven into fabrics to provide a soft and flexible material or coated in a matrix and aligned to form uni-directional plies, which are then stacked and pressed under temperature and pressure to form rigid laminates. The manufacturing process from UHMW-PE to fibre-reinforced composite laminates is shown schematically in Figure 2.3. In the case of DSM Dyneema®, UHMW-PE composite is supplied as uni-directional prepreg already consolidated into a thin [0/90]\(_2\) laminate as standard, which in this thesis is referred to as a “layer”. UHMW-PE composites and fabrics have been shown to be extremely effective against ballistic threats, particularly in weight-critical applications, e.g. personal protection vests and helmets for protection against small calibre threats (Cunniff, 1999) and as contact spall liners (Ong et al. (2011) and O’Masta et al. (2014)).
2.2 Ballistic Properties

This section reviews our current understanding of the response of UHMW-PE composite under ballistic impact. Firstly the ballistic regime is defined to set the scope for the impact problems investigated in this thesis. It is important to understand the behaviour of the load-bearing component of the composite, i.e. the fibres, under transverse impact, before delving into the response of the full-scale composite under impact. As such this section is arranged so that fibre theories are presented first. This is followed by a review of the ballistic impact performance of both thin and thick UHMW-PE composite panels. The key energy absorption mechanisms identified in literature are described followed by a brief review of the influence that projectile and interlaminar properties have on the ballistic performance of UHMW-PE composite.

From this point onwards a distinction is made in regards to defining thin and thick composite panels. General observations of UHMW-PE composite under ballistic impact show that it exhibits a transition in failure mechanism as the thickness of the panel is increased. As such, thin panels are defined as undergoing a single stage of perforation, while thicker panels are defined by targets perforated in two or more stages.

2.2.1 Ballistic Regime

Historically, impact events are classified according to the impact velocity. One such approach was proposed by Zukas et al. (1982) who categorised impact problems based on impact velocity, where the material response and established strain rate characterised the impact problem. Figure 2.4 depicts the strain rate, the impact velocity required to achieve the strain rate and the material effects as proposed by Zukas et al. (1982).
Under this classical approach, the ballistic regime could be considered to be within the strain rate range of $10^2$ s$^{-1}$ to $10^4$ s$^{-1}$ which corresponds to an impact velocity of between 50 m/s to 3000 m/s. According to Zukas et al. (1982), within this region the material strength is important in resisting penetration, and there is an onset of hydrodynamic effects. This approach to define the impact regime using the strain rate is insufficient when considering the impact behaviour for a diverse range of materials with different properties. For example, very low velocity impact of a hard object into a fluid can be described entirely using hydrodynamic theory, which under Figure 2.4 would be classified as both a low strain rate (low velocity) and high strain rate (hydrodynamic effects) problem. Wilbeck (in Anderson et al., 2013a) proposed classification by the ratio of the impact pressure ($P$) to the material strength ($S$).

$$\frac{P}{S_p} \quad \text{and} \quad \frac{P}{S_t}$$

(2.1)

where subscripts $p$ is the projectile and $t$ is the target. The pressure, $P$, according to hydrodynamic theory is given by:

$$P \approx \rho V^2$$

(2.2)

where $\rho$ is material density and $V$ is the impact velocity. From this, impact events can be classified in nine different regimes, depicted in matrix form in Figure 2.5.
The ballistic regime is considered to reside in regime 2, 4, 5, 6 and 8 according to Figure 2.5 where the impact pressure is close to the material strength of the target, projectile or both. For this thesis, regime 5, 6 and 8 are of interest because at impact velocities typical of FSPs, the impact pressures are considered to be on the order of, or greater than the strength of UHMW-PE composite.

### 2.2.2 UHMW-PE Fibre, Yarn and Fabrics

Smith et al. (1958) provided a theoretical foundation to the underlying physics of yarns under transverse impact. They showed that when a yarn is impacted transversely, two longitudinal strain waves propagate and travel away from the impact point. Between these two wave fronts, strain is assumed to be uniform. This was later proved to be accurate by Walker and Chocron (2011). The material between the two wave fronts moves in towards the impact point, forming a transverse wave in the shape of a 'V' propagating at a constant velocity that depends on the wave speed of the material. For material with a linear elastic stress-strain curve (a good approximation for most fibres including UHMW-PE (Russell et al., 2013), aramid (Cheng et al., 2005), glass and carbon (Kant and Penumadu, 2014) at high strain rates), yarn theory reduces to the following simple equations:

\[
V = c \sqrt{\varepsilon \left( 2 \sqrt{\varepsilon (1 + \varepsilon)} - \varepsilon \right)} \tag{2.3}
\]

\[
U = c \left( \sqrt{\varepsilon (1 + \varepsilon)} - \varepsilon \right) \tag{2.4}
\]

\[
W = c \varepsilon \tag{2.5}
\]

where \( V \) is the impact velocity, \( U \) is the transverse wave velocity, \( W \) is the velocity of the yarn
moving inwards towards the impact point, $c$ is the elastic wave speed and $\varepsilon$ is the strain in the yarn. For a linear elastic fibre filament, the wave speed is:

$$c = \sqrt{\frac{E}{\rho}}$$  \hspace{1cm} (2.6)

where $E$ is the elastic modulus. These equations have been studied and applied extensively, and have been found to be very accurate at predicting the transverse wave velocity for a range of yarns, including UHMW-PE (Chocron et al., 2011). It however over-predicts the critical velocity (Bazhenov et al. (2001) and Chocron et al. (2011)), which is the impact velocity that develops strain in the fibre equal to the fibre failure strain, leading to an almost instantaneous rupture. Walker and Chocron (2011) show that yarns fail at lower speeds than those predicted by the theory, due to the yarn bouncing off the projectile surface when initially impacted. This initial bounce causes a momentary acceleration of the yarn beyond the projectile velocity, that leads to failure of the yarn below the critical velocity predicted using Smith et al. (1958) theory. In contrast, Carr (1999) studied the failure mechanism of Dyneema® SK66 yarn under ballistic impact by a 0.68 g steel sphere. Carr (1999) found Dyneema® yarn exhibited two failure modes depending on the impact energy. At low impact energy, the yarn failed due to the transmitted stress waves, while at higher impact energy the yarn failed through shearing and exhibited melting. This shows that the transverse impact loading on fibres is a complex phenomena involving wave mechanics and different failure modes.

Cunniff (1999) derived a set of dimensionless parameters that allow different fabric systems to be compared based on their mechanical properties. The dimensional ratios are:

$$\frac{V_{50}}{(U^*)^{1/3}} \text{ and } \frac{AD_t A_p}{m_p}$$  \hspace{1cm} (2.7)

where $AD_t$ is the target areal density, $A_p$ is the projectile presented area and $m_p$ is the projectile mass. $U^*$ is the normalising velocity and is defined as:

$$U^* = \frac{S \varepsilon_{max}}{2\rho} \sqrt{\frac{E}{\rho}}$$  \hspace{1cm} (2.8)

where $S$ is the tensile strength of the fibre, $\varepsilon_{max}$ is the fibre failure strain, $E$ is the fibre elastic modulus and $\rho$ is the fibre density. This term is a combination of the specific toughness and the acoustic wave speed of the fibre. Cunniff (1999) showed that the normalised velocity scaling term is a good predictor of the relative ballistic performance of different fabric systems. Table 2.1 compares the specific toughness, wave speed and normalised velocity, $U^{*1/3}$, for a range of fibre materials.

<table>
<thead>
<tr>
<th>Strength (GPa)</th>
<th>Failure Strain (%)</th>
<th>Modulus (GPa)</th>
<th>Density (kg/m³)</th>
<th>Specific Toughness (kJ/kg)</th>
<th>Wave Speed (km/s)</th>
<th>$U^{*1/3}$ (m/s)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Polybenzobisoxazole (PBO)</td>
<td>5.2</td>
<td>3.1</td>
<td>169</td>
<td>1560</td>
<td>51.7</td>
<td>10.4</td>
</tr>
<tr>
<td>UHMW-PE (Spectra 1000)</td>
<td>2.57</td>
<td>3.5</td>
<td>120</td>
<td>970</td>
<td>46.4</td>
<td>11.1</td>
</tr>
<tr>
<td>Aramid (Kevlar KM2)</td>
<td>3.4</td>
<td>3.55</td>
<td>82.6</td>
<td>1440</td>
<td>41.9</td>
<td>7.6</td>
</tr>
<tr>
<td>Carbon</td>
<td>3.8</td>
<td>1.76</td>
<td>227</td>
<td>1800</td>
<td>18.6</td>
<td>11.2</td>
</tr>
<tr>
<td>E-Glass</td>
<td>3.5</td>
<td>4.7</td>
<td>74</td>
<td>2550</td>
<td>32.3</td>
<td>5.4</td>
</tr>
</tbody>
</table>

Walker (1999) and Phoenix and Porwal (2003) proposed analytical models to describe the perforation of fabric targets based on the assumption of a membrane loaded transversely. Both models show that the performance of fabrics is dependent on $AD_t A_p/m_p$, the acoustic wave
speed and the failure strain of the yarn. Chocron et al. (2011) used finite element analysis for a single yarn and multilayer fabrics. Using only the tensile properties of the yarn, and assuming linear elastic behaviour, they were able to model with good accuracy the transverse wave velocity (as predicted using Smith et al. (1958) theory, equation 2.4), the strain in the yarn (using embedded NiCr wire as strain gauges) and the ballistic limit of Kevlar® KM2, Dyneema® SK65 and PBO fabrics.

2.2.3 Thin UHMW-PE Composite

The performance of thin composite laminates has been investigated by a number of researchers, and remains a more active area of research compared to thick composite panels. This is because the primary application of UHMW-PE composite today is in personal protection (helmets and vests) where there are limitations in thickness and weight and the threat condition is significantly less than the weight and threat levels in vehicle applications. The transverse wave speed of uni-directional composite strips and single layers of [0/90]_2 (as supplied by the manufacturer) of Dyneema® HB80 (Dyneema® SK76 fibers and polyurethane matrix) was investigated by Chocron et al. (2013). Observations show that the presence of the matrix has no effect on the transverse wave speed of the composite, i.e. wave propagation for the dry fiber and composite are the same and predictable using Smith et al. (1958) theory (Equation 2.4). The penetration and perforation of thin UHMW-PE composite involves many deformation and failure mechanisms. Thin laminates (consisting of only several plies) fail by fibre-matrix debonding and fibre pull-out, rather than fibre straining to failure (Lee et al. (1994), Katz et al. (2008) and Chocron et al. (2013)). Lee et al. (1994) studied the failure mechanism of Spectra fibre composites (areal densities between 0.5 kg/m^2 to 12 kg/m^2) under ballistic impact by a 5.56 mm FSP and identified several failure mechanisms. They observed delamination failure increasing through the composite thickness, cut-out of a plug by through-thickness shear, and combined shear and tensile failure of the fibres. These mechanisms were also identified by Flanagan et al. (1999) and Karbalaie et al. (2011). Furthermore, Flanagan et al. (1999) subjected the composite to impact velocities significantly above the threshold perforation velocity and found penetration was mainly through shear plugging, with the rear layers failing in tension. In contrast, Karthikeyan and Russell (2014) investigated the deformation and failure mechanisms of UHMW-PE composite between 0.75 mm to 5 mm thick against spherical projectiles and found two stages of failure for targets thicker than 1 mm. They argue that thicker targets initially experience fibre fracture due to an indirect tension mechanism (discussed in more detail in section 2.4.3) brought about by compression of the composite, resulting in shear stresses between adjacent plies and tensile stresses in the fibres (Attwood et al., 2014). When the pressure at the projectile target interface reduces below the levels required to initiate indirect tension failure, no more fibre fracture occurs and the target is stretched through membrane action.

Morye et al. (1999) compared the ballistic performance of consolidated UHMW-PE prepreg and hot compaction of dry woven fibres without resin. The two laminates were found to have similar ballistic performance but absorbed impact energy through different mechanisms. Conventional prepreg laminates absorbed energy via fibre fracture whereas compacted composites absorbed energy through a combination of fibre failure and back face delamination. Peijs et al. (1994) compared the impact energy of a drop weight impact test on Spectra® composites to the energies associated with fibre fracture and delamination, and found that up to 90% of the impact energy is absorbed by fibre fracture while the remaining 10% is due to delamination. Under ballistic impact the number of broken fibres/yarns has been strongly correlated to the penetration resistance of the composite, indicating that membrane stretching is the dominant energy absorption mechanism under ballistic impact (Walsh et al. (1998), Zhang et al. (1998),
Zee and Hsieh (1998) and Lee et al. (2001)). The kinetic energy of the accelerating membrane was also found to account for a significant portion of the total impact energy (Morye et al., 2000). Cunniff (1992) and Karthikeyan and Russell (2014) have shown that thin fabrics and composites with fewer layers are more effective and have higher energy absorption capacity than thicker targets. This was attributed to higher bending stiffness in targets with more layers as the presence of the adjacent layers reduced deflection, which reduced the overall effectiveness of each individual layer.

A summary of the available ballistic limit ($V_{50}$) data from literature is shown in Figure 2.6 for a range of different UHMW-PE grades against 5.56 mm FSPs, 12.7 mm spherical projectiles and 20 mm FSPs. Figure 2.6(a) shows the vast majority of tests were performed for targets less than 10 mm thick, with only Heisserer and Van der Werff (2012) reporting results for thicker targets against large calibre FSPs. When the ballistic limit velocity is plotted with respect to the non-dimensional areal density ratio as proposed by Cunniff (1999) in Figure 2.6(b), the data is found to collapse on to a straight line for Dyneema® HB26, HB50 and Spectra® 1000 against 5.56 mm and 20 mm FSP. HB26 and HB50 use the same grade of UHMW-PE fibres (Dyneema® SK76) but use a different matrix material, polyetherdiol-aliphatic diisocyanate polyurethane (PADP) and styrene-isoprene-styrene triblock copolymer (SISTC) respectively (Attwood et al., 2014). The data does not collapse for Dyneema® HB212, as this contains a different grade of UHMW-PE fibre with a higher tensile (but with same SISTC matrix as HB50). The data also does not collapse for the spherical projectile suggesting that the non-dimensional areal density parameter is only comparable for projectiles of similar geometry.

Figure 2.6: Summary of ballistic limit data of UHMW-PE from literature in terms of (a) target thickness and (b) non-dimensional areal density

### 2.2.4 Thick UHMW-PE Composite

Thick UHMW-PE composite exhibits more than one stage of perforation, i.e. there is transition from one deformation and failure mechanism to another. The earliest studies on the penetration mechanisms of thick UHMW-PE composite were reported in Taylor and Carr (1999) and Iremonger (1999). Taylor and Carr (1999) performed post-failure analysis of ballistic impacted UHMW-PE composite at two thicknesses, 7 mm and 25 mm, impacted by a 1.1 g steel ball and various 7.62 mm Armour Piercing (AP) NATO ammunition. Taylor and Carr (1999) found that the 25 mm targets exhibited complete delamination at a distance of 50% to 70% of the thickness of the target from the impact face with the back face displaying gross deformation.
Significant fibre and matrix melting was observed in these tests with some evidence of fibre shearing. Iremonger (1999) studied the ballistic impact of UHMW-PE composite up to 32 mm thick against full metal jacketed 5.56 mm calibre L2A2 and L3A1 bullets. Iremonger (1999) described the penetration of thick UHMW-PE composite in three stages. In the first stage the projectile shears through the material with minimal energy loss. In the second phase, the projectile is destabilised and is deformed, which is accompanied by delamination of the panel. In the final stage, the rear sub-laminate bulges allowing the fibres to be loaded in tension. Cross-sections of the targets showed minimal deflection of the thin target (11 mm thick) while targets greater than 22 mm displayed significant bulging of the back face (Iremonger, 1999).

Heisserer et al. (2013) studied the depth of penetration of 25 mm thick UHMW-PE composite against 6 mm diameter aluminium spheres and found an almost linear relationship between the depth of penetration and the impact kinetic energy. Greenhalgh et al. (2013) conducted fractographic analysis of two 20 mm thick UHMW-PE composite bonded together with adhesive tape impacted by a 20 mm copper FSP. This investigation found delamination was dominant away from the impact site. Furthermore they showed UHMW-PE composite exhibits a sequence of failure modes including delamination, ply splitting and fibre kinking and also found that the entry and exit halves of the panel behaved independently, though this was affected by the built-in delamination between the two halves of the target (Greenhalgh et al., 2013). O’Masta et al. (2014) compared the performance of an aluminium plate covered by Dyneema® HB26 on the front, back or both faces to investigate its performance as part of a hybrid armour system. This investigation concluded that UHMW-PE improves the ballistic performance of the system when placed on the back of the aluminium plate due to the pre-acceleration of the rear Dyneema® plate. This was initiated by bulging deformation of the aluminium target, which reduced the relative velocity between the aluminium and the Dyneema® and therefore contact pressure of the projectile on the Dyneema®.

The penetration mechanisms of thick glass (GFRP), aramid (AFRP) and nylon fibre-reinforced composites was investigated by Woodward et al. (1994). Although a different class of composite to UHMW-PE, these studies provide insights to the response of thick fibre-reinforced composites to ballistic impact. Woodward et al. (1994) performed depth of penetration tests on semi-infinite targets of S2 glass, Kevlar® 29 and ballistic nylon fibre-reinforced composites and found that the composites exhibited different deformation and failure mechanisms. Nylon and Kevlar® exhibited ballistic penetration by stretching, fracture, withdrawal by elastic recovery and extrusion of fibre while GFRP showed significant fibre crushing and fracture below the projectile face and significant delamination and up-flow of material around the projectile. Gellert et al. (2000) studied the effect of target thickness on the ballistic perforation of GFRP, and showed that the protection efficiency of GFRP increases with thickness. This is because the high initial resistance to bending of thicker targets causes the target to be penetrated initially by indentation and compression until the matrix phase at the ply interface begins to fracture, leading to dishing of the rear face. For GFRP, the indentation phase was shown to be responsible for the increased ballistic performance of thicker targets relative to thinner targets that exhibit only the dishing mechanism.

The ballistic performance of thick UHMW-PE composite has received relatively little attention compared to thin composites. Ballistic limit tests on thick UHMW-PE composite have been reported by Heisserer and Van der Werff (2012). However these tests were performed on large calibre 20 mm FSPs, so the target to projectile areal density ratio is still relatively small (see Figure 2.6(b)). Furthermore, although the multistage penetration of thick UHMW-PE composite has been identified by a number of authors, none have investigated how this changes with thickness or impact velocity. Taylor and Carr (1999) and Greenhalgh et al. (2013) observed mid-plane delamination of thick targets, but this was not investigated and the mechanism...
responsible for this is still not well understood. These aspects have significant implications for the ballistic performance of thick UHMW-PE composites and warrant further investigation.

2.2.5 Influence of Projectile on Ballistic Performance

Different projectile shapes and materials can invoke different penetration and failure mechanisms on the target material. For composites and fabrics, the majority of literature is focused on aramid such as Twaron® and Kevlar® (Montgomery et al. (1982), Tan et al. (2003), Steier et al. (2014) and Gibbon et al. (2014)). These studies found that more energy is absorbed by the target against a hemispherical or flat-ended projectile compared to an ogive or conical projectile (Tan et al. (2003) and Gibbon et al. (2014)). This is because flat-ended projectiles typically shear through the target breaking more fibres in the process, as compared to conical projectiles that are able to push fibres aside as they penetrate through the target. Montgomery et al. (1982) found that blunt projectiles are more effective at perforating the target at lower velocities and pointed projectiles have more effect at higher impact velocities. At lower impact velocities, it was found that pointed projectiles are more unstable and had a tendency to yaw upon impact. At higher impact velocities, the projectile is more stable, yawing was reduced upon impact, and the projectile was more effective at penetrating the target. Montgomery et al. (1982) found that this effect diminished as targets thickness increased. Steier et al. (2014) investigated the effect of projectile material on the ballistic performance of aramid fabrics. This study found that energy absorbed by the target was not affected by the impacts of spherical projectiles made from steel, aluminium, brass, alumina, glass, rubber and nylon.

Tan and Khoo (2005) provide the only study investigating the ballistic impact of UHMW-PE composite by projectiles of different geometry. The study investigated impacts with steel flat-ended, hemispherical, ogive and conical projectiles of the same mass against 0.18 mm thick unidirectional cross-ply Spectra® 1000 laminates. They observed flat-ended projectiles perforated the laminate through shearing of the material around the perimeter of the projectile, while hemispherical projectiles perforated the laminate by tensile straining of the fibres in direct contact with the projectile. Ogive and conical projectiles were observed to penetrate by pushing the fibres aside laterally, causing deflection of the fibres around the projectile tip. These observations are similar to those already seen for aramid fabrics and composites.

2.2.6 Influence of Resin Material on Ballistic Performance

The non-polar chemical structure of polyethylene molecules leads to intra-chain bonding via very weak Van der Waals forces. As such, the fibre-matrix adhesion in UHMW-PE composite is weak leading to very poor intra- and inter-laminar properties. However, weak fibre-matrix interfacial properties have been correlated to improved ballistic performance (Kirkland et al. (1991), Frissen (1996) and Karbalaie et al. (2011)). According to Kirkland et al. (1991) the weak adhesion allows energy dissipation through the breaking of the fibre-matrix interface.

The transverse wave propagation velocity of UHMW-PE composite strips was investigated by Chocron et al. (2013). Although the presence of the matrix was considered to act as a dead weight and reduce the transverse wave velocity, no effect on the transverse wave speed was found in these tests. They attribute this unexpected result to the low fibre-matrix bonds that lead the matrix to detach from the fibres very quickly upon impact, reducing or eliminating its effect on the transverse wave velocity. The effect of reinforcing the fibres with resin on the ballistic performance of composites was investigated by Walker (2001). Walker (2001) argued
that four mechanisms come into play when fibres are reinforced by a matrix phase:

1. bending stiffness increases the target resistance to deformation, leading to improvements in ballistic performance;
2. the increased bending stiffness leads to an increase in the transverse wave velocity, reducing the strain and increasing the ballistic performance;
3. the target hardness increases, deforming the projectile more, increasing the presented area of the projectile and therefore increasing the ballistic performance;
4. fibres are more rigidly fixed, allowing the projectile to shear the fibres rather than failing them in tension (optimal failure model), therefore reducing ballistic performance.

The Walker (2001) analytical model showed that for Kevlar-based fabrics and composites, dry fabrics have higher ballistic performance for lower areal densities because of the loss of fibre mass (and therefore tensile strength) when adding resin to maintain the same areal density. For higher areal densities, the ballistic performance of composites was higher due to the increase in bending stiffness.

Lee et al. (1994) compared the influence of matrix material on the ballistic performance of UHMW-PE composite. They found that a stiffer matrix resulted in better ballistic performance and impact fatigue life because composites with a stiffer matrix absorb more energy through development of larger delamination zones. A stiffer matrix also reduces fibre mobility near the projectile, engaging more fibres in the impact event (so more energy is absorbed), improving the impact resistance (Walsh et al. (1998) and Lee et al. (2001)). The dynamic response to a soft impact of UHMW-PE composite beams with different matrix material was investigated by Karthikeyan et al. (2013a). They found higher failure impulse for composites with the lower matrix strength. The ballistic performance of UHMW-PE and carbon fibre composite containing the same fibres but different matrix was compared in Karthikeyan et al. (2013b). This study found that the ballistic performance of the composite with the lower matrix strength was higher, although the mechanisms describing this process were not explained in this work.

The consolidation pressure, temperature and time have been shown to affect the porosity and therefore fibre-matrix interface properties of UHMW-PE composite (Greenhalgh et al., 2013). Karbalaie et al. (2011) studied the ballistic performance of UHMW-PE composite produced under a range of consolidation temperatures, pressures and times. Consolidation temperature was found to have the largest effect on the ballistic performance of the composite, with time and pressure having only a small influence. Morye et al. (1999) studied the influence of consolidation pressure on the mechanical and ballistic properties of UHMW-PE composite and found that laminates consolidated at higher consolidation temperatures provided higher interlaminar strength but lower ballistic performance. Greenhalgh et al. (2013) studied the energy absorption and fracture mechanisms of UHMW-PE composite produced under two consolidation pressures. They report an increased prevalence of delamination (predominantly mode II fracture driven), particularly towards the back of the target for targets consolidated under higher pressures. The higher consolidation pressure reduced the interply resin thickness and increased the interlaminar voidage, leading to enhanced mode I fracture toughness (by promoting fibre bridging) and reduced mode II fracture toughness (Greenhalgh et al., 2013). For this reason, they observed an increase in the prevalence of mode I driven delamination in targets consolidated under lower pressures. The relative ballistic performance of these test cases was not discussed in this work.
2.3 Analytical Models

Analytical models of ballistic performance are important for a more complete understanding of the governing mechanisms through a mathematical description of the key penetration processes. They also allow for an approximation of the material ballistic performance, which is extremely useful for design and analysis. In this section, existing analytical models describing the penetration of fabrics and fibre-reinforced composites are reviewed. Although the focus of this thesis is on fibre-reinforced composites, fabric models are still relevant as they can describe the response of the load-bearing fibres under ballistic impact. Several relevant analytical models are applied to UHMW-PE composite in this section and are assessed on their accuracy in predicting the ballistic limit of UHMW-PE composite compared to experimental results from literature.

Analytical models can be categorised in two broad categories: energy conservation-based derivations or momentum conservation-based derivations. Analytical models derived from energy conservation are those that consider the energy balance between the projectile kinetic energy and the various energy absorption mechanisms of the target. This approach was adopted by Morye et al. (2000), Wen (2001), Billon and Robinson (2001), Naik et al. (2006) and Bresciani et al. (2015) to describe fabrics and fibre-reinforced composites. Analytical models based on momentum conservation use some form of Newton’s second law of motion to describe the force and momentum exchange in the system. Researchers including Chocron-Benloulo et al. (1997c), Chocron-Benloulo et al. (1997b), Walker (1999) and Phoenix and Porwal (2003) have used this method to describe the ballistic impact of fabrics and fibre-reinforced composites. Some researchers have developed momentum equations where an iterative calculation method is required to sum the contribution of each individual ply or layer (Vinson and Zukas (1975), Parga-Landa and Hernandez-Olivares (1995), Mamivand and Liaghat (2010) and Chen et al. (2013)). The vast majority of analytical models to date have been developed to describe the ballistic impact of woven fabrics. Some of this work includes Vinson and Zukas (1975), Chocron-Benloulo et al. (1997c), Walker (1999), Billon and Robinson (2001), Phoenix and Porwal (2003), Mamivand and Liaghat (2010) and Chen et al. (2013). Some models are modified to account for the additional response of composites under impact, such as the inclusion of bending resistance in Walker (2001) or delamination in Chocron-Benloulo et al. (1997b). Aside from these two analytical models, there are relatively few analytical models describing the ballistic impact of fibre-reinforced composites. This work includes Morye et al. (2000), Gellert et al. (2000), Wen (2001), Naik et al. (2006) and Bresciani et al. (2015). Compared to fabric models, these composite models describe the additional effect of bending, delamination and matrix cracking which are prevalent in fibre-reinforced composites.

A summary of many of the analytical models reviewed here is presented in Table 2.2. It shows that most fabric models are based on momentum theory, while most composite models are based on energy theory. This is because mechanisms such as delamination, matrix cracking, etc., in composites are more readily described using energy terms, e.g. delamination modes are best characterised by the fracture energy. On the other hand, fabric models are generally based on yarn stress wave propagation theory such as Smith et al. (1958). These equations solve for the strain in the yarn upon transverse impact, which allows calculation of the stress and hence the reaction force, making the momentum approach more suitable.
Table 2.2: Analytical models

<table>
<thead>
<tr>
<th>Model</th>
<th>Material</th>
<th>Equation Validation</th>
<th>Material Validation</th>
</tr>
</thead>
<tbody>
<tr>
<td>Vinson and Zukas (1975)</td>
<td>Fabric Momentum</td>
<td>Nylon</td>
<td></td>
</tr>
<tr>
<td>Chocron-Benloulo et al. (1997-99)</td>
<td>Fabric Momentum</td>
<td>Aramid (Kevlar® 29)</td>
<td></td>
</tr>
<tr>
<td>Walker (1999)</td>
<td>Fabric Momentum</td>
<td>Aramid (Kevlar® KM2)</td>
<td></td>
</tr>
<tr>
<td>Billon and Robinson (2001)</td>
<td>Fabric Energy</td>
<td>Nylon, aramid and high modulus polyethylene</td>
<td></td>
</tr>
<tr>
<td>Phoenix and Porwal (2003)</td>
<td>Fabric Momentum</td>
<td>Aramid (Kevlar® 29 &amp; KM2), PBO and UHMW-PE (Spectra® 1000)</td>
<td></td>
</tr>
<tr>
<td>Mamivand and Langhate (2010)</td>
<td>Fabric Momentum</td>
<td>Aramid (Kevlar® 29)</td>
<td></td>
</tr>
<tr>
<td>Chen et al. (2013)</td>
<td>Fabric Momentum</td>
<td>Aramid (Kevlar® 49)</td>
<td></td>
</tr>
<tr>
<td>Chocron-Benloulo et al. (1997b)</td>
<td>Composite Momentum</td>
<td>UHMW-PE (Dyneema® UD SK66)</td>
<td></td>
</tr>
<tr>
<td>Morey et al. (2000)</td>
<td>Composite Energy</td>
<td>Nylon, aramid and UHMW-PE (Dyneema® UD SK66)</td>
<td></td>
</tr>
<tr>
<td>Gellert et al. (2000)</td>
<td>Composite Energy</td>
<td>Glass (E-glass)</td>
<td></td>
</tr>
<tr>
<td>Walker (2001)</td>
<td>Composite Momentum</td>
<td>Aramid (Kevlar® 29)</td>
<td></td>
</tr>
<tr>
<td>Wen (2001)</td>
<td>Composite Energy</td>
<td>Glass (S2-glass &amp; E-glass) and aramid (Kevlar® 29)</td>
<td></td>
</tr>
<tr>
<td>Naik et al. (2006)</td>
<td>Composite Energy</td>
<td>Glass (E-glass)</td>
<td></td>
</tr>
<tr>
<td>Bresciani et al. (2015)</td>
<td>Composite Energy</td>
<td>Aramid (Kevlar® 29) and glass (E-glass)</td>
<td></td>
</tr>
</tbody>
</table>

Aside from the model proposed by Gellert et al. (2000), all of the models listed in Table 2.2 describe the penetration of thin fabrics or fibre-reinforced composites. These models can include multiple failure and energy absorption mechanisms, however they only describe the penetration event in a single stage. For example, Naik et al. (2006) and Bresciani et al. (2015) derived equations describing the different energy absorption mechanisms of fibre-reinforced composites including deformation and failure of primary yarns, deformation of secondary yarns, delamination and matrix cracking, kinetic energy of the bulge, shear plugging, etc. These mechanisms are grouped together and act under a single penetration stage. The ballistic performance was determined by comparing the total energy absorbed by the composite to the initial kinetic energy of the projectile. In contrast, Gellert et al. (2000) made a distinction between thick and thin composite targets, where thick composites exhibit multiple stages of penetration and thin composites exhibit only a single penetration stage. For GFRP, Gellert et al. (2000) proposed a model that describes an initial indentation phase, which is then followed by a dishing phase where the work to strain and fail the composite in tension, delamination and bending were considered.

The application of the Gellert et al. (2000) model to thick UHMW-PE composite is however problematic because there are many constants that have not been determined for UHMW-PE composite and more importantly, it is questionable whether the mechanisms for GFRP composite described in this model are applicable to thick UHMW-PE composite. Similarly, many of the other analytical models listed in Table 2.2 either required aspects of the ballistic properties to be known, e.g. bulge radius in Morey et al. (2000) or reference ballistic results in Chocron-Benloulo et al. (1997b), or require an iterative method to solve. The models developed by Walker (1999), Walker (2001) and Phoenix and Porwal (2003) however are closed form equations that do not require anything more than the projectile and fibre properties. The model derived in Walker (1999) is a spring-based model that is used to describe fabrics under membrane stresses and was validated for aramid-based fabrics. The ballistic limit velocity from this model is given by the following equation:

\[
V_{BL} = \frac{9}{2} \left( 1 + \beta^2 \frac{AD_t A_p}{m_p} \right) c_f \varepsilon_{max} \left\{ \left( \frac{r_{BL}}{r_p} \right)^{2/3} - 2 \left( \frac{r_{BL}}{r_p} \right)^{1/3} + 3 \right\}^{-1} \quad (2.9)
\]

where \( \beta \) is an area multiplier that accounts for the fact that under membrane loading, momentum transfer from the projectile to the target occurs over an area larger than the projectile presented area (otherwise shear plugging occurs). \( AD_t \) is the target areal density, \( A_p \) is the
projectile presented area, $m_p$ is the projectile mass, $c_f$ is the longitudinal wave speed of the fibre, $\varepsilon_{max}$ is the failure strain of the fibre and $r_{BL}/r_p$ is the ratio of the radius of the fabric involved in the ballistic impact to the radius of the projectile and is given by:

$$\frac{r_{BL}}{r_p} = \sqrt[3]{\frac{9\pi}{8} \left( \frac{m_p}{ADtAp} + \beta^2 \right)} \quad (2.10)$$

This model was extended for composites to include the influence of the matrix and its effect on the bending resistance of the composite (Walker, 2001). The model was validated for Kevlar® epoxy composites and is given by the equation:

$$V_{BL} = \frac{9}{2} \sqrt{1 - R + R \left( \frac{\beta ADtAp}{m_p} \right)^3 \left( 1 + \beta^2 \frac{ADtAp}{m_p} \right) c_f \varepsilon_{max} \left\{ \left( \frac{r_{BL}}{r_p} \right)^{2/3} - 2 \left( \frac{r_{BL}}{r_p} \right)^{1/3} + 3 \right\}^{-1}}$$

where $R$ is the mass fraction of the matrix or resin.

Phoenix and Porwal (2003) developed a fabric model based on yarn deformation under membrane loading that was validated against a range of fabrics including Kevlar®, Spectra® and PBO. The ballistic limit velocity for this model is:

$$V_{BL} = \left( 1 + \beta^2 \frac{ADtAp}{m_p} \right) c_f \left( \frac{\varepsilon_{max}}{K_{max} \left( \varepsilon_{max}, \frac{ADtAp}{m_p} \right)} \right)^{3/4} \quad (2.12)$$

where $K_{max}$ is the strain concentration factor as a function of the failure strain $\varepsilon_{max}$ and the non-dimensional areal density ratio $ADtAp/m_p$ and is given by:

$$K_{max} = \frac{\varepsilon}{\varepsilon_{max}} \approx \exp \left\{ - \frac{4\beta^2 \frac{ADtAp}{m_p} \left( \psi^2 - 1 \right)}{3 \left( 1 + \beta^2 \frac{ADtAp}{m_p} \right)} \right\} \psi^{1/3} \left( \frac{\sqrt{\psi/\varepsilon_{max}} (\psi - 1)}{\ln \left\{ 1 + \sqrt{\psi/\varepsilon_{max}} (\psi - 1) \right\}} \right)^{2/3} \quad (2.13)$$

where $\psi$ is the ratio of the radius of the conical wave front to the radius of the projectile, given by:

$$\psi = \frac{r_c}{r_p} \approx \sqrt{\frac{1 + \beta^2 \frac{ADtAp}{m_p}}{2\beta^2 \frac{ADtAp}{m_p}}} \quad (2.14)$$

As a benchmarking exercise, Equations 2.9, 2.11 and 2.12 are used to calculate the ballistic limit velocity of UHMW-PE composite against 5.56 mm and 20 mm FSPs. Here the input parameters are taken from high strain rate tests of UHMW-PE yarns (Dyneema® SK76) from Russell et al. (2013) ($\varepsilon_{max} = 0.02$, $c_f = \sqrt{E/\rho} = \sqrt{131 \times 10^9/980} = 11562$ m/s) and $\beta$ is 1.4, taken as an average of the values used in Walker (1999), 1.5, and Phoenix and Porwal (2003), 1.3. The results are plotted in figure 2.7 and are compared to experimental data from literature. The results are plotted in terms of the non-dimensional areal density.
All three analytical models provide good predictions of the ballistic performance, although above $AD_t A_p/m_p$ of approximately 0.1, the results deviate and under-predict the ballistic limit velocity. These three analytical model are all based on membrane theory, which allows them to predict the performance of thin targets with reasonable accuracy. Also, because the ballistic limit velocity is predicted accurately using these models with only the fibre failure strain and longitudinal wave speed as the material input parameters, it suggests that these parameters are the most important for the ballistic performance of thin targets. Clearly as the target thickness increases relative to the size and mass of the projectile, the target undergoes a different penetration mechanism that is not captured by these membrane-based analytical models. The results suggest that this transition occurs for $AD_t A_p/m_p$ of between 0.1 and 0.15. Further investigations are required for targets beyond this $AD_t A_p/m_p$ to identify the penetration mechanisms of thick targets. This is important for derivation of an analytical model for thick UHMW-PE composite targets.

### 2.4 Mechanical and Shock Response of UHMW-PE Composite

#### 2.4.1 Tension

The tensile properties of UHMW-PE fibre are the most important material properties that determine the ballistic performance of UHMW-PE composite, as discussed extensively in sections 2.2 and 2.3. The tensile properties of UHMW-PE fibres vary widely depending on the applied manufacturing process, temperature and strain rate. The influence of strain rate and temperature will be discussed in subsequent sections. The draw ratio (rate at which the gel solution is extended to produce fibre filaments) has a large influence on the molecular orientation in the fibres and hence significantly affects the tensile properties of the fibres (Ward, 1985). Higher draw ratios tend to lead to more uniform micro-structures with less defects giving rise to higher...
material strength, stiffness and reduced creep rate (Ward (1985) and Berger et al. (2003)). Under laboratory conditions, tensile strengths and elastic moduli of up to 7.5 GPa and 160 GPa have been achieved using advanced gel-spinning techniques at very high draw ratios (Savitskii et al. (1984), Hoogsteen et al. (1988) and Pennings et al. (1990)). Table 2.3 compares the tensile properties of some commercial grades of UHMW-PE fibres.

Table 2.3: Tensile properties of commercial UHMW-PE fibre (Van Dingenen, 2001)

<table>
<thead>
<tr>
<th>Manufacturer</th>
<th>Yarn Type</th>
<th>Density (kg/m³)</th>
<th>Linear Mass Density (dtex)</th>
<th>Tenacity (N/tex)</th>
<th>Strength (GPa)</th>
<th>Modulus (GPa)</th>
<th>Elongation to Break (%)</th>
</tr>
</thead>
<tbody>
<tr>
<td>DSM Dyneema® SK60</td>
<td>970</td>
<td>3.85</td>
<td>3.4</td>
<td>3.3</td>
<td>120</td>
<td>116</td>
<td>3.8</td>
</tr>
<tr>
<td>Honeywell Spectra® 1000</td>
<td>970</td>
<td>5.5</td>
<td>3.2</td>
<td>3.1</td>
<td>110</td>
<td>107</td>
<td>3.3</td>
</tr>
<tr>
<td>Honeywell Spectra® 2000</td>
<td>970</td>
<td>1.1</td>
<td>2.6</td>
<td>2.5</td>
<td>75</td>
<td>73</td>
<td>3.6</td>
</tr>
<tr>
<td>Honeywell Spectra® 3000</td>
<td>970</td>
<td>1.1</td>
<td>3.3</td>
<td>3.3</td>
<td>120</td>
<td>116</td>
<td>3.8</td>
</tr>
</tbody>
</table>

Discrepancies in the tensile performance of UHMW-PE fibre can exist even when comparing across the same grade of fibres as the material behaviour can depend on the test configuration. A comparison between the tensile performance of UHMW-PE yarns was conducted using steel rollers and bonded rubber sheets in Russell et al. (2013). These experiments found significant differences between the strength measurements in the two different setups. Heisserer (2013) found that single UHMW-PE fibre tensile tests were also prone to slippage between the grips. This is because the low friction coefficient of UHMW-PE fibre makes it susceptible to slippage when using mechanical grips or adhesive methods. These differences can also be attributed to the complex load distribution within the fibre (Berger et al., 2003). Berger et al. (2003) found that due to the imperfect microstructure of UHMW-PE fibre, only six per cent of the molecular chains are loaded to capacity before rupturing, while the bulk of the fibre experiences inter-molecular and micro-fibrillar slip under tensile loads.

Characterisation of the tensile performance of UHMW-PE composite laminates has been particularly problematic. In order to derive meaningful results from a tension test, two conditions should be satisfied: uniform stress in the gauge length and no specimen slippage from between the grips. The low coefficient of friction and poor adhesion between the fibre-matrix interface of UHMW-PE composite makes testing with standard rectangular specimens with end tabs as described in ASTM D3039 (ASTM International, 2013) ineffective. Poor adhesion of end tabs on straight-ended specimens has been observed to cause premature specimen failure due to the delamination and slippage of the end tabs (Morye et al. (1999), Iannucci et al. (2009) and Levi-Sasson et al. (2014)). Similarly untabbed straight edge and dog-bone specimens held using hydraulic grips exhibited specimen slippage and premature failure due to delamination failure of the outer plies (Iannucci et al. (2009), Heisserer (2013) and Levi-Sasson et al. (2014)). This is followed by progressive failure of the inner plies and results in a change in slope or plateau region on the stress-strain curve. This behaviour is illustrated in Figure 2.9(a) with the test performed by Heisserer (2013) using ISO dogbone specimens (as shown in Figure 2.8(a)). Figure 2.9(b) shows how the tensile strength is affected by the specimen thickness. In this case, the thicker the specimen, the earlier the onset of failure, and the lower the measured tensile strength. To address the issue of specimen slippage, Russell et al. (2013) proposed a through-bolted dogbone specimen, which was also conceptually adopted in Levi-Sassoon et al. (2014) and Lässig et al. (2015), the specimen geometry is shown in Figure 2.8(b). These test specimens are similar to dogbone specimens, although at the gripping area, the specimen is sandwiched between metal plates and bolted together. Tensile tests performed using these specimens typically yield higher tensile strengths than those using the standard dogbone specimen, as shown in 2.9(b). If the rule of mixtures is applied and it is assumed that the 90 degree plies and the
matrix do not contribute to the strength of the laminate, the theoretical tensile strength of a laminate containing 80 percent fibre volume fraction of Dyneema® SK76 fibres (properties from Table 2.3), is approximately 1.44 GPa \((S_t \times v_f) / 2 = (3600 \times 0.8) / 2\). However, the tensile strength measured using the through-bolted dogbone specimen is still well below the theoretical value. Although these through-bolted specimens resolve gripping issues, the problem of uniform stress distribution in the gauge length still remains. This is because the fibre-matrix adhesion and interlaminar properties of the laminate are too low to effectively transfer the applied load throughout the laminate, so progressive failure of the specimen still persists, as shown in Figure 2.9(a) from test performed by Levi-Sasson et al. (2014).

The large influence of specimen thickness on the measured tensile strength can be attributed to specimen slippage and non-uniform load distribution in the gauge length. This is most noticeable when comparing the dogbone results from Heisserer (2013) and Czechowski et al. (2012) in Figure 2.9(b). Heisserer (2013) also used special textile mechanical rollers (cartan grip fixture, Figure 2.8(c)) to determine the tensile properties of a thin UHMW-PE composite laminate \([0/90]_2\). In this test, a long thin specimen is wound around mechanical rollers multiple
times such that the friction between the wound material is sufficient to prevent slipping when a load is applied by displacement of the rollers. Since all of the plies are directly loaded using this fixture, a more uniform stress distribution is achieved and the problem of axial stress transmission is resolved. Using this method, Heisserer (2013) found tensile strength of 1151 MPa with a standard deviation of 54.6 MPa. This value is much closer to the theoretical value with a stress-strain curve that exhibits near linear-elastic rise followed by instantaneous failure, i.e even load distribution and collective failure in the gauge section.

2.4.1.1 Strain Rate Effects

Application of UHMW-PE fibre and laminates in ballistic armour where the material is subjected to very high strain rates, has led many researchers to investigate its properties under high strain rates (Govaert and Lemstra (1992), Kromm et al. (2003), Berger et al. (2003), Huang et al. (2004), Koh et al. (2008), Koh et al. (2010) and Russell et al. (2013) amongst others. As a highly orientated and crystalline thermoplastic material, UHMW-PE fibre exhibits visco-plastic stress strain behaviour (Kromm et al., 2003). Under tensile creep loading the deformation behaviour of UHMW-PE fibre is found to be the result of two contributions: a reversible (visco-elastic) component from straining at the molecular level, and an irreversible plastic flow contribution related to the inter-molecular, micro- and macro-fibrillar slip (Govaert and Lemstra (1992) and Berger et al. (2003)). Under creep loading, Berger et al. (2003) found that chain slipping was dominant while long chain rupture (of the covalent bonds) was a minor failure mechanism.

The high strain rate characterisation of UHMW-PE yarn and laminates has been studied using a tensile Split Hopkinson Pressure Bar (SHPB) or Kolsky Bar by a range of researchers, including Chocron-Benloulo et al. (1997a), Huang et al. (2004), Koh et al. (2008) and Koh et al. (2010). The SHPB is a method to characterise the material response at strain rates up to $10^4$ s$^{-1}$ based on one-dimensional wave propagation theory. Application of the technique to soft materials such as UHMW-PE yarn and laminates poses some challenges due to the large impedance mismatch between the bar and the test material that causes significant noise and attenuation of the wave signal (Chen et al., 1999). To address these issues, Chocron-Benloulo et al. (1997a), Koh et al. (2008) and Koh et al. (2010) used specially designed clamps to reduce the impedance-mismatch between the steel bar and the UHMW-PE test sample. Typically these tests show that the dynamic tensile properties of UHMW-PE laminates exhibit higher modulus, strength and lower strain to failure when compared to quasi-static properties (Chocron-Benloulo et al. (1997a), Koh et al. (2008) and Koh et al. (2010)). The degree of variation between different test methods in these papers is substantial, making comparability to quasi-static measurements difficult.

Russell et al. (2013) developed a new method of testing the tensile response of fibre yarn for a range of strain rates between $10^{-4}$ s$^{-1}$ to $10^9$ s$^{-1}$. Their method involved an anvil that is connected to a piezoelectric force sensor by a loop of yarn. The anvil is loaded by either a servo-hydraulic test machine or impulsively by a projectile depending on the strain rates required, while strain is measured by digital image correlation of the elongation between two markers on the yarn that is captured by high speed camera. Unlike previous methods, this method allows direct comparison between the range of strain rates. Russell et al. (2013) found the tensile performance of UHMW-PE yarns (Dyneema® SK76) is highly sensitive to strain rate when subjected to strain rates below $10^{-1}$ s$^{-1}$. This agrees with Kromm et al. (2003) who tested fibres (Dyneema® SK75) between a strain rate of $10^{-5}$-$10^{-2}$ s$^{-1}$. Beyond a strain rate of $10^{-1}$ s$^{-1}$, the tensile strength, strain to failure and elastic modulus is insensitive to the applied load up to a strain rate of $10^3$ s$^{-1}$. Russell et al. (2013) attributed the strain rate dependency of UHMW-PE fibres below strain rates of $10^{-1}$ s$^{-1}$ to creep of the fibres. As the strain rate increase, straining of the molecular
chain becomes more dominant, reducing the strain rate sensitivity of the fibres. Furthermore as the strain rate increases, the response of the fibres becomes more linear-elastic (Russell et al., 2013), indicative of reversible loading of the molecular chain. The effect of the strain rate on the strength, failure strain and elastic modulus of UHMW-PE composite is shown in Figure 2.10 (taken from Russell et al. (2013)). The large variations in results between different researchers is due to different test methodologies and fibre grades used.

The fracture morphology of UHMW-PE fibre has been shown to demonstrate brittle failure at high strain rates and ductile failure at low strain rates (Peijs et al. (1994) and Govaert and Peijs (1995)). These findings were confirmed in Koh et al. (2008) where the fracture morphology of UHMW-PE composite was examined at different strain rates. Under quasi-static tensile loads, fibres exhibit ductile failure where the fibre displays significant plastic deformation and necking at the failure point. This failure mechanism, as discussed above, is due to the irreversible slipping of the molecular chains and micro-fibrils through failure of the secondary (Van der Waals) bonds. Koh et al. (2008) found that the failure morphology become predominantly brittle at strain rates of $400 \text{ s}^{-1}$, as fibres exhibit a clean cleavage fracture surface. Brittle failure is associated with the failure of the primary (covalent) molecular bonds. Interestingly, Koh et al. (2008) observed decreased failure strength and modulus and increased failure strain at strain rates of $850 \text{ s}^{-1}$. They postulated that this may be due to frictional and visco-elastic effects at very high strain rates, which causes the local temperature between the contacting molecules and fibrils to rise. This leads to material softening and promotes ductile failure and a greater failure strain at high strain rates.

High strain rate tension tests on laminates have only been performed using servo-hydraulic machines at high displacement rates. These machines are typically limited to a displacement rate of 1000 mm/min, which depending on the specimen geometry can achieve strain rates in the order of $10^3 \text{ s}^{-1}$ in tensile loading. Although this is several orders of magnitude below the strain rates typically observed under ballistic impact, the fact that the load-bearing fibres are strain rate insensitive above $10^1 \text{ s}^{-1}$ according to Kromm et al. (2003) and Russell et al. (2013), suggests that characterisation of the laminate up to a strain rate of $10^1 \text{ s}^{-1}$ may be sufficient for ballistic applications. Plotted in Figure 2.11 are tensile strength results of through-bolted dogbone specimens at various strain rates from Russell et al. (2013), Lässig (2012), Levi-Sasson et al. (2014) and Czerwinski (2013). Although the through-bolted dogbone specimen is susceptible to uneven load distribution at the gauge length due to poor fibre-matrix adhesion and low interlaminar properties (as discussed previously), this comparison is made because they all use similar specimens and test methodologies. Similar to what is observed at the fibre level in Figure 2.10, Figure 2.11 shows the tensile strength of the laminate increasing with increasing
strain rate up to about $10^{-1}$ s$^{-1}$, and then appearing to plateau off thereafter. More test data is required at high strain rates to confirm this observation, however none currently exist in the scientific literature.

![Graph showing tensile strength vs strain rate](image.png)

Figure 2.11: The effect of strain rate on the tensile strength of UHMW-PE composite from through-bolted dogbone specimens

2.4.1.2 Temperature Effects

Under ballistic impact, a temperature rise in the material often occurs that leads to thermal softening and melting of the composite (Morye et al. (1999), Greenhalgh et al. (2013) and Chocron et al. (2013)). The temperature rise is due to the irreversible process of plastic deformation and shock induced heating that leads to energy lost in the system in the form of heat. As such the effect of temperature rise on the material response of UHMW-PE composite is critically important and has been the subject of significant interest from the scientific community. Dijkstra et al. (1989) investigated the response of UHMW-PE fibre to a quasi-static tensile load over a wide range of temperature between -150°C to 150°C and found a failure transition at approximately 20°C. This was found to be due to the stress-induced orthorhombic-hexagonal phase transition above 20°C which allows greater movement and slippage of the molecular chains leading to failure by creep. Below 20°C, an orthorhombic phase exists that makes it more difficult for chain slippage to occur, leading to loading of the covalent-bonds in the polyethylene chain.

Dessain et al. (1992) conducted similar tests and investigated the temperature effect on the elastic modulus, strength and failure strain of UHMW-PE fibres. Further studies on the temperature effect on UHMW-PE fibres and yarn in the creep and quasi-static regimes was conducted in Peijs et al. (1994), Govaert and Peijs (1995), Devaux and Caze (1999a) and Kromm et al. (2003). Huang et al. (2004) investigated the response of UHMW-PE fibre bundles at 25°C and 70°C at strain rates of 300 s$^{-1}$ and 700 s$^{-1}$. Marais and Feillard (1992), however, studied the influence of temperature on the elastic modulus of Dyneema® SK60 composite over a temperature range of -50°C to 120°C under quasi-static loading. All of these studies show the strength and modulus decrease and strain-to-failure increases with increasing temperature. Furthermore
Peijs et al. (1994) and Govaert and Peijs (1995) showed that the work-to-fracture increases with increasing temperature or decreasing strain rate.

Figure 2.12 shows the effect of temperature on the tensile properties of UHMW-PE fibres from Dessain et al. (1992). For temperatures between 0°C to 100°C, there is significant reduction in the tensile strength of UHMW-PE fibres. This is critical because this temperature range is within the operating environment for which UHMW-PE composite is used as an armour material. Furthermore, shock, friction and plastic deformation induced heating observed in ballistic testing (Greenhalgh et al. (2013) and Chocron et al. (2013)) can lead to a significant degradation of material strength and therefore ballistic performance.

The failure morphology of UHMW-PE fibre also changes based on the temperature. Similar to the effect of strain rate, UHMW-PE fibres experience predominantly ductile failure (plastic deformation and necking at the fracture location) at high temperatures and brittle fracture (clean cleavage surface) at low temperatures (Peijs et al., 1994). The increase in the mobility of the molecular chains with increased temperature is attributed to a predominantly ductile failure as the resistance to failure of secondary Van der Waals bonding reduces (Peijs et al., 1994).

2.4.2 Shear

Shear properties of Dyneema® SK76 fibre were investigated by Hudspeth et al. (2012), using a torsion pendulum apparatus. By rotating the fibre to a known level of torsional induced strain, and measuring the angular acceleration of an attached disc pendulum upon release, Hudspeth et al. (2012) determined the shear stress of the fibre for a given shear strain based on angular momentum theory. A biaxial tension-shear failure surface was defined using this method at high strain rates showing a fibre shear strength of 1.9 GPa. This method assumes a uniform fibre cross-section and an isotropic material. In reality UHMW-PE fibres are composed of smaller micro- and macro-fibrils (Berger et al., 2003) that fibrillate when rotated to a high shear strains. Fibrillation causes deflection, rather than permanent deformation of the fibres and so stress-strain calculations based on a cylinder in torsion would be affected. Despite this a number of researchers have used this torsion pendulum method to determine the shear modulus of single fibres made from glass, nylon, aramid, PBO and carbon (Deteresa et al. (1984), Allen (1988) and Mehta and Kumar (1994)), but not for UHMW-PE fibre.
UHMW-PE fibre is used with a number of different resins to produce a composite laminate. These resins include epoxy, polyethylene, polypropylene, polydicyclopentadiene amongst others (Peij et al. (1990), Marais and Feillard (1992) and Devaux et al. (2002)). More recently UHMW-PE fibres manufactured from DSM Dyneema® are typically combined with a polyurethane or a rubber-based polymer known as Kraton® (Karthikeyan et al., 2013b). Due to the non-polar molecular structure of UHMW-PE, bonding between UHMW-PE fibre and the resin material is generally poor (Peij et al. (1990), Devaux et al. (2002), Katz et al. (2008) and Russell et al. (2013)). The interfacial shear strength between polyethylene reinforced by UHMW-PE fibres was investigated in Devaux and Caze (1999b) using fibre pullout tests. They show that the interfacial shear strength is improved if a chemically comparable material is used and further performance increases can be achieved through appropriate fibre surface treatment.

The in-plane intralaminar shear properties of UHMW-PE composite were characterised in Iannucci et al. (2009), Russell et al. (2013), Heisserer (2013), Levi-Sasson et al. (2014), Nazarian and Zok (2014b), Pullen et al. (2015) and Lässig et al. (2015). Russell et al. (2013), Heisserer (2013) and Lässig et al. (2015) performed tests on through-bolted dogbone specimens with plies angled at [+45/-45] similar to those specified in ASTM D3518 (ASTM International, 2007), while Levi-Sasson et al. (2014) used a V-notched specimen loaded in a rail shear fixture as specified in ASTM D7078 (ASTM International, 2005). Under small shear strains, both the V-notch and 45 degree uniaxial tension tests are expected to produce almost pure shear response (Sims, 1973). However the 45 degree uniaxial tension test has been found to be inappropriate for measuring the in-plane shear strength due to fibre reorientation at high shear strains (Wisnom, 1995). These tests show highly non-linear stress-strain response that initially is dominated by matrix shearing, followed by a phase of orientation hardening as a result of matrix yielding and fibre realignment to the loading direction (Nazarian and Zok, 2014a). Russell et al. (2013) compared the in-plane shear performance for strain rates between $10^{-4}$ s$^{-1}$ to $10^{-2}$ s$^{-1}$ and found the properties are highly strain rate sensitive. Table 2.4 summarises the in-plane shear properties of Dyneema® HB26 as measured by various authors.

<table>
<thead>
<tr>
<th>Source</th>
<th>Specimen</th>
<th>Strain Rate (s$^{-1}$)</th>
<th>$G$ (MPa)</th>
<th>$\tau_{\text{max}}$ (MPa)</th>
<th>$\varepsilon_{\text{max}}$ (rad)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Russell et al. (2013)</td>
<td>45 deg tension</td>
<td>$10^{-4}$</td>
<td>60</td>
<td>28</td>
<td>0.28</td>
</tr>
<tr>
<td>Heisserer (2013)</td>
<td>45 deg tension</td>
<td>$10^{-2}$</td>
<td>-</td>
<td>120</td>
<td>0.40</td>
</tr>
<tr>
<td>Levi-Sasson et al. (2014)</td>
<td>V-notched</td>
<td>$10^{-4}$</td>
<td>81.6</td>
<td>-</td>
<td>-</td>
</tr>
<tr>
<td>Pullen et al. (2014)</td>
<td>45 deg tension</td>
<td>$10^{-4}$</td>
<td>-</td>
<td>38</td>
<td>0.55</td>
</tr>
<tr>
<td>Lässig et al. (2015)</td>
<td>45 deg tension</td>
<td>$10^{-4}$</td>
<td>35.3</td>
<td>35</td>
<td>0.53</td>
</tr>
</tbody>
</table>

While slight variations in strain rate might influence the shear modulus, strength and failure strain, there is significant difference in these properties as shown in Table 2.4. These differences may also be due the dependency of the shear properties on the specimen geometry (Kellas et al., 1993) or the consolidation time, temperature and pressure, which is related to the porosity level of a composite (Ye et al., 1995). A high level of porosity has extensively been shown to significantly affect the mechanical properties of a composite (Bowlies and Frimpong (1992), Wisnom et al. (1996) and Mouritz (2000)). The studies by Iannucci et al. (2009), Russell et al. (2013), Heisserer (2013), Levi-Sasson et al. (2014), Nazarian and Zok (2014a), Pullen et al. (2015) and Lässig et al. (2015) on the in-plane shear properties of UHMW-PE composite all had variations in specimen geometry and the consolidation condition was not specified.

The interlaminar shear properties of UHMW-PE composite were measured by Chocron et al. (2014) using a lap shear specimen. This study investigated the specimen with different consolidation pressures and hydrostatic pressures. The results showed negligible difference from
specimens consolidated between 20.7 MPa and 27.6 MPa but a significant influence when the hydrostatic pressure was varied. Chocron et al. (2014) measured a shear strength of approximately 2.6 MPa under atmospheric pressure and a linear increase to about 8 MPa under 500 MPa of hydrostatic pressure as shown in Figure 2.13. The increase in shear strength under increasing hydrostatic pressure is because the initiation and propagation of cracks is inhibited under hydrostatic pressure (Vyas et al., 2011).

![Figure 2.13: Influence of hydrostatic pressure on interlaminar shear strength (Chocron et al., 2014)](image)

Test methods based on the short beam shear and lap shear test are typically used to characterise the matrix-dominated interlaminar shear properties of the composite. However, ballistic impact can cause localised shear failure of the fibres at very high impact velocities or for very thick targets as discussed in the previous sections (section 2.2.4) and in Nguyen et al. (2016). As such it is important for the through-thickness shear properties of the bulk material (fibre and matrix) to be characterised. Since UHMW-PE composite is composed of 80% fibre volume fraction or more, the bulk properties are critically important. Lässig et al. (2015) used a modified short beam shear setup that combines the four point bend and rail shear test to induce localised shear stress in the test specimen. The method characterises the through-thickness shear properties of composites from the elastic regime all the way to final failure and has been applied to Kevlar® epoxy composite (Riedel et al., 2003a) and CFRP (Wicklein et al., 2007). Due to a limitation in the maximum displacement of the fixture however, Lässig et al. (2015) was unable to characterise the through-thickness shear property to complete failure for UHMW-PE composite. Furthermore in this test it was found that under large shear strains fibre realignment does occur. These tests indicate that through-thickness shear loading must occur at higher strain rates in order to avoid fibre realignment.
2.4.3 Through-Thickness Compression

Under ballistic impact, a pressure wave develops and propagates through the material. This pressure wave places the material ahead of the projectile under very high compressive load. For composites such as GFRP, the resistance to through-thickness compression was identified to be a dominant energy absorption mechanism (Woodward et al., 1994). The compression of UHMW-PE fibres has been observed experimentally during ballistic impact experiments (Nguyen et al., 2015). The lateral compression properties of UHMW-PE fibres have not been reported in the scientific literature, however such properties have been investigated for Kevlar® fibre, which also has a micro-fibrillar microstructure. Phoenix and Skelton (1974), Cheng et al. (2004) and Cheng et al. (2005) investigated the transverse compression properties of Kevlar® by a force-displacement curve and a theoretical treatment of the fibre deformation. These studies show elastic-plastic behaviour and a transverse compressive modulus approximately two orders of magnitude lower compared to the fibre longitudinal modulus. Furthermore, Phoenix and Skelton (1974) found that Kevlar® filaments remained intact even under large transverse compressive strains while graphite filaments were reduced to powder under similar compressive loads. Cheng et al. (2004) and Cheng et al. (2005) investigated the response of Kevlar® fibre to bi-axial transverse compression and longitudinal tension and found increases in the transverse compressive stiffness of the fibres, which can be favourable for ballistic impact.

The through-thickness compressive properties of UHMW-PE composite have recently been a subject of significant interest (Chocron et al. (2014), Attwood et al. (2014), Pullen et al. (2015) and O’Masta et al. (2015)). Chocron et al. (2014) investigated the effect of hydrostatic pressure and consolidation pressure on the through-thickness compression properties of UHMW-PE composite and found a small influence from consolidation pressure (higher consolidation pressure leads to higher stiffness and strength in compression) but a large effect due to hydrostatic pressure (higher hydrostatic pressure results in higher stiffness and strength under compression). Pullen et al. (2015) investigated the influence of strain rate and found increased compressive stiffness at higher strain rates. The failure mechanisms of UHMW-PE composite under out-of-plane compression was investigated in Attwood et al. (2014). This study showed the through-thickness compression strength increased with increased in-plane specimen dimensions, matrix and fibre strength, but decreased with increasing ply thickness. These properties are interrelated because under an out-of-plane compression load, Attwood et al. (2014) found UHMW-PE composite (with a cross-ply layup) developed tensile stresses in the plies through a shear lag mechanism. Here a shear lag regime develops when the material is compressed, causing axial extrusion due to the Poisson effect that leads to tensile stress in the plies and shear stresses at the ply interfaces. Due to a finite inter-ply shear strength, extrusion of each ply and its neighbours is impeded, leading to a build up of in-plane tensile stresses. Figure 2.14, taken from Attwood et al. (2014) shows how the tensile stresses develop under a compressive force. Attwood et al. (2014) compared this with uni-directional UHMW-PE composite, which showed no fibre tensile stress and a compressive strength significantly lower than the cross-ply composite.
O’Masta et al. (2015) deduced the effect of two types of defects from processing cross-ply UHMW-PE composite on the compression properties. This study found that tunnel cracks developed from anisotropic thermal contraction had no effect on the compressive properties, but the existence of void-like defects from missing groups of fibres significantly degraded the compressive strength of the composite.

2.4.4 Shock Properties

The shock response of a material plays an important role in the ballistic performance at high impact velocities. For a detailed explanation of the development of shocks and its effect on the material properties, the reader is referred to Meyers (1994) and Hiermaier (2007). A brief overview of shocks and their effects is described below. For a given compression state, the material sound speed is proportional to the adiabatic compression modulus \( \left( \frac{\partial P}{\partial v} \right) \). As the pressure increases, the material volume decreases, increasing the material sound speed. The velocity of the pressure wave propagation is higher as the pressure increases, leading to superposition of the low speed waves under initial compression with late time high amplitude fast waves. A shock front develops as a result that causes a discontinuity in the pressure, volume (density) and internal energy (temperature) across the shock. There are two conditions that must be met in order for a shock to form. The first is that the slope of the pressure-volume curve must be negative \( \left( \frac{\partial P}{\partial v} \right) < 0 \). The second is that the applied load must be fast enough such that the pressure increment does not reach equilibrium before the faster wave front is able to superimpose on the initial wave front (Hiermaier, 2007). The pressures and loading rate under high velocity ballistic impact are generally sufficient to form shocks, which affect the pressure, volume (density) and internal energy (temperature) state of the material. Figure 2.15 shows the state variables that define the condition before and after the passage of a shock wave. The relationship between the material pressure \( P \), volume \( v \) (density \( \rho \)) and internal energy \( e \) (temperature \( T \)) is described by the equation of state (EoS).
For solids, the EoS is generally described with a baseline of known pressure-energy-density conditions. This can be theoretical using statistical mechanics (such as the zero Kelvin isothermal), or more commonly, experimental through characterisation of the material Hugoniot curve. These reference conditions are used in a general EoS, such as Mie-Grüneisen, Tillotson, p-α etc. to determine volumetric state of the material under shock loading (Hiermaier, 2007). The Hugoniot curve, also known as the shock Hugoniot or shock adiabat, describes all possible peak conditions upon passage of a shock wave. The Hugoniot curve is not an equation of state however, nor is it a path along different shock states. It only represents one specific curve in the state surface of the material. The Hugoniot curve is often depicted in the shock velocity $U_s$ and particle velocity $u_p$ (velocity behind the shock) plane. The $U_s$-$u_p$ relationship is generally characterised from plate impact experiments where the shock and particle velocity can be measured or inferred. In general the $U_s$-$u_p$ relationship is defined by an arbitrary linear or polynomial function:

$$U_s = c_o + S u_p$$  \hspace{1cm} (2.15)

$$U_s = c_o + S u_p + S_2 u_p^2$$ \hspace{1cm} (2.16)

where $c_o$ is the bulk sound speed and $S$ is the slope (coefficients) of the curve. Under very high shock pressures, the polynomial function provides a more accurate description of the shock and particle relationship for polymers including GFRP and CFRP (Appleby-Thomas and Hazell, 2012):

Together with the Rankine-Hugoniot equations, the $U_s$-$u_p$ relationship can be used to determine the pressure, volume (density) and internal energy (temperature) state of the material under shock loading. The Rankine-Hugoniot equations describes the conservation of mass, momentum and energy across a shock front:

Mass \hspace{1cm} $\rho_0 U_s = \rho_1 (U_s - u_p)$ \hspace{1cm} (2.17)

Momentum \hspace{1cm} $\rho_0 U_s u_p = P_1 - P_0$ \hspace{1cm} (2.18)

Energy \hspace{1cm} $P_1 u_1 = (e_1 - e_0) \rho_0 U_s + \frac{1}{2} \rho_0 U_s u_1^2$ \hspace{1cm} (2.19)

The inverse flyer plate impact (IFPI) test (shown schematically in Figure 2.16), has been used to characterise the shock response of a number of anisotropic materials with arbitrary compression and compaction properties including concrete (Riedel et al., 2008), Kevlar® epoxy (Riedel et al., 2003a) and CFRP (Wicklein et al., 2007). The method is able to characterise the shock response of the material at very high pressures and strain rates $10^6 \text{s}^{-1}$ to $10^7 \text{s}^{-1}$. The general procedure is as follows:
The test specimen is mounted to a sabot with a backing plate. The specimen is impacted at $V_I$ onto another plate of known shock properties, termed the witness plate (subscript $w$).

- A laser interferometer (such as a velocity interferometer system for any reflector or VISAR) is placed behind the witness plate to measure the free surface velocity $u_{fs}$ on the back of the sample.

- A shock wave is initiated in both plates upon impact and is reflected at the back surface causing a velocity jump. By measuring the $V_I$ and $u_{fs}$ and knowing the $U_s-u_p$ relation for the witness plate, the shock properties of the test specimen can be determined, where subscript $H$ denotes the Hugoniot state:

$$u_p = V_I - \frac{1}{2}u_{fs}$$

$$\sigma_H = \rho_{o,w}c_{o,w} \left( \frac{1}{2} u_{fs} \right) + \rho_{o,w}S_w \left( \frac{1}{2} u_{fs} \right)^2$$

$$U_s = \frac{\sigma_H}{\rho_0 u_p}$$

$$e - e_0 = \frac{1}{2} \left( \frac{1}{\rho_0} - \frac{1}{\rho} \right) \sigma_H = \frac{1}{2} u_p^2$$

$$\frac{\rho_0}{\rho} = \frac{v}{v_0} = \frac{U_s - u_p}{U_s}$$

$$\varepsilon_H = \frac{u_p}{U_s}$$

- The test is repeated for varying impact velocities and a plot of the $U_s-u_p$ relationship is obtained.

Typically the shock Hugoniot of composite materials is used with the Mie-Grüneisen EoS (Hayhurst et al. (1999), Clegg et al. (2006) and Wicklein et al. (2008)), to provide a first order approximation of the material volumetric state. The Mie-Grüneisen EoS provides a relationship between the pressure, volume and internal energy of a material:

$$P(v, e) = P_H + \frac{\Gamma(v)}{v} (e - e_H)$$
where $\Gamma (v)$ is the Grüneisen coefficient. There are a number of ways to determine $\Gamma (v)$, which are discussed in Dugdale and MacDonald (1953), Nagayama and Mori (1994) and Meyers (1994).

Characterising the shock Hugoniot is therefore critically important in deriving an EoS for UHMW-PE composite. A number of researchers have performed inverse planar plate impact tests on UHMW-PE composites. Hayhurst et al. (2000) conducted IFPI tests on Dyneema® HB25. They used the VISAR system to measure the free surface velocity of a C45 steel witness plate. Chapman et al. (2009) also used the VISAR system to characterise the response of Dyneema® (within a polyethylene matrix) to shock loading and compared its shock response against standard polyethylene in Carter and Marsh (1995) and Millett and Bourne (2004). They found the $U_s-u_p$ relationship of Dyneema® lies below that of standard polyethylene despite Dyneema® having a higher crystallinity and density. Hazell et al. (2011) characterised the shock response of Dyneema® (with a polyethylene matrix) in the fibre direction using strain gauges to measure the shock velocity and the impedance match method to determine the particle velocity. Their results showed little difference between the Hugoniot in the fibre direction as compared to the through-thickness direction. This was due to using materials of similar chemical composition within the reinforcement and matrix phase. In contrast Millett et al. (2007) studied the effect of orientation on the shock response of CFRP and found different responses in the fibre and through-thickness directions at low impact pressures. The shock response in the fibre direction was found to be greater. At very high stresses, the response between the loading directions converges and there is no orientation effect on the material shock properties. Furthermore, Hazell et al. (2011) observed that under elevated shock stresses sufficient to initiate melting (approximately 3.5 GPa), the elastic precursor wave disappears as melting of the fibre and matrix prevents the elastic precursor wave from separating and forming ahead of the shock. Lässig et al. (2015) characterised the shock properties of Dyneema® HB26 in the through-thickness direction with results similar to that of Chapman et al. (2009) and Hazell et al. (2011) suggesting the polyurethane matrix has negligible orientation effects in Dyneema® composites. Figure 2.17 summarises the $U_s-u_p$ and pressure-density relationship for UHMW-PE composite.

![Figure 2.17: Hugoniot properties of UHMW-PE composite in (a) the shock-particle velocity plane and (b) pressure-density plane](image)

In Figure 2.17(a), a linear $U_s-u_p$ function is shown to be adequate for capturing the shock-particle velocity relationship of UHMW-PE composite over the available experimental results. Figure 2.17(b) shows the pressure-density relationship of UHMW-PE composite, calculated from equation 2.22 and 2.24 above and the $U_s-u_p$ data. The impact velocities achieved in these
inverse planar plate impact tests reach a pressure ($\sigma_H$) of 3.5 GPa on the first wave pass, which is similar to the tensile strength of UHMW-PE fibres. The results show UHMW-PE composite exhibits a non-linear pressure-density response, i.e. a disproportionately higher pressure is required to hydrostatically compress the material. A non-linear equation of state is therefore essential for any applications involving high impact velocities or pressures for this material.

2.5 Numerical Models

2.5.1 Hydrocode

Predictive numerical tools can be extremely useful for enhancing our understanding of ballistic impact events. Models that are able to capture the key mechanical and thermodynamic processes can significantly improve our understanding of the phenomena by allowing time-resolved investigations of virtually every aspect of the impact event. Such high fidelity is immensely difficult, prohibitively expensive or near impossible to achieve with existing experimental measurement techniques.

Hydrodynamic codes or hydrocodes are a category of continuum mechanics codes that attempt to describe the dynamics of continuous media through the application of the principles of conservation equation of mass, momentum and energy (Anderson, 1987).

\[
\frac{\partial \rho}{\partial t} + V_i \frac{\partial \rho}{\partial x_i} + \rho \frac{\partial v_i}{\partial x_i} = 0
\]  
\[
\frac{\partial V_i}{\partial t} + V_j \frac{\partial V_i}{\partial x_j} = f_i + \frac{1}{\rho} \frac{\partial}{\partial x_j} (\sigma_{ij} V_i)
\]  
\[
\frac{\partial e}{\partial t} + V_i \frac{\partial e}{\partial x_i} = f_i V_i + \frac{1}{\rho} \frac{\partial}{\partial x_j} (\sigma_{ij} V_i)
\]

where $\rho$ is the density, $V$ is the velocity, $e$ is the specific total energy, $\sigma_{ij}$ is the stress tensor, and $f$ is the external body force. The subscripts represent standard tensorial notation. The equations are solved numerically via explicit discretisation in space (finite element, finite difference, finite volume or mesh-free methods) and time. The kinematic deformation of continuous media can be described in the Eulerian (spatial) and Lagrangian (material) framework (Anderson, 1987):

Lagrangian:

\[
\frac{d\rho}{dt} + \rho \frac{\partial v_i}{\partial x_i} = 0
\]  
\[
\frac{dV_i}{dt} = f_i + \frac{1}{\rho} \frac{\partial}{\partial x_j} (\sigma_{ij} V_i)
\]  
\[
\frac{de}{dt} = f_i V_i + \frac{1}{\rho} \frac{\partial}{\partial x_j} (\sigma_{ij} V_i)
\]

Eulerian:

\[
\frac{\partial \rho}{\partial t} + \frac{\partial}{\partial x_i} (\rho V_i) = 0
\]  
\[
\frac{\partial V_i}{\partial t} + V_j \frac{\partial V_i}{\partial x_j} = f_i + \frac{1}{\rho} \frac{\partial}{\partial x_j} (\sigma_{ij} V_i)
\]
Energy \[ \frac{\partial e}{\partial t} + V_i \frac{\partial e}{\partial x_i} = f_i V_i + \frac{1}{\rho} \frac{\partial}{\partial x_j} (\sigma_{ij} V_i) \tag{2.35} \]

The first attempt at solving hydrodynamic problems involving material response under large distortion and compression in more than one dimension was performed in Evans and Harlow (1957). Bjork (1958) used the method developed in Evans and Harlow (1957) to study the hypervelocity impact of steel impacting steel and aluminum. These early hydrocodes were based on hydrodynamic theories because the impact velocities studied were so high that the impact pressures were considerably higher than the material strength. However the strength of the material becomes an important consideration in the late stage of hypervelocity impacts as the impact pressure decays down to the order of the material strength. Riney (1963) and Wilkins and Giroux (1963) were the first to formulate and incorporate a constitutive model into hydrocodes to describe material behaviour in this regime.

For impact problems where strength is important, the Lagrangian formulation is favored as the method allows the history of material boundaries to be traced, leading to better treatment of constitutive and failure models compared to the Eulerian formulation (Anderson, 1987). Since the material is moving with the grid/mesh, large distortions of the grid/mesh can occur, leading to a divergence of the cell accuracy, and cell reversal which results in negative mass and extremely small time steps. Two methods have been proposed in order to resolve the issues of large grid distortion. This includes rezoning of the grid in the area of highly distorted elements (Hertel, 1993) and element deletion or erosion (Johnson and Stryk, 1987). These methods are still problematic however, because rezoning is computationally expensive while element erosion does not represent the physics of the impact event.

Relatively newer numerical discretisation methods such as Smoothed Particle Hydrodynamics (SPH), a mesh free method derived from the Lagrangian formulation have been proposed that rectifies the issue of grid entanglement (Gingold and Monaghan (1977) and Lucy (1977)). SPH was extended for applications in solid mechanics by Libersky and Petschek (1990). The SPH method has shown good agreement with high velocity impact of metallic targets (Schraml and Kimsey, 1998), better predictions of crack propagation in ceramics (Hiermaier and Riedel, 1997) and fragmentation of composites under hypervelocity impact (HVI) (Hiermaier et al. (1999), Riedel et al. (2003a) and Wicklein et al. (2007)) compared to grid-based Lagrange and Euler methods. Although promising, SPH suffers from consistency and stability issues that lead to lower accuracy and instabilities under tensile perturbation (Hiermaier, 2007). The latter makes it unsuitable for use with UHMW-PE composite under ballistic impact, because this material derives most of its resistance to penetration when it is loaded in tension. For these types of problems, the grid-based Lagrangian formulation still remains the most feasible for modelling UHMW-PE composite.

### 2.5.2 Fibre-Reinforced Composite Modelling

Modelling of fibre-reinforced composites under impact is challenging because of the complexity of the material composition and the many failure modes it exhibits at different scales (fibrillation, intra- and inter-laminar failure, etc.) and impact regimes. For this reason numerical simulation of impact using hydrocodes was exclusively performed for isotropic materials up until the late 1990’s. Since then, there have been many advances in modelling composites brought about by the introduction of more accurate constitutive models and modelling techniques. In general fibre-reinforced composites can be modelled at three different scales, as shown in figure 2.18:
- Micro-scale, where the individual fibre, matrix and (in some cases) the fibre-matrix interface is explicitly modelled;

- Meso-scale, where the properties of the individual plies that are homogenised in the principal directions are modelled and stacked together to produce a laminate; and

- Macro-scale, where the laminate is modelled as a continuum and the properties of the laminate are homogenised in the principal directions.

![Micro-scale Meso-scale Macro-scale](image)

Figure 2.18: Micro, meso and macro mechanical model of fibre reinforced composites (Meyer and Mayer, 2010)

Modelling of fibre-reinforced composites at the micro-scale has several important advantages. This includes increased model fidelity, relatively simpler constitutive equations to describe the fibre, matrix and the interface, and characterisation tests that are relatively easy to perform. However models at this scale require explicit modelling of every single fibre, matrix and the contact interface, which is extremely computationally expensive and not practical currently for typical engineering problems. While the meso-scale approach is far more computationally tractable compared to the micro-scale, models at this scale are still not practical for thick targets, which is the focus of this thesis. With the increasing development of high speed computers however, some researchers have modelled UHMW-PE composite under ballistic impact at the micro- and meso-scale very recently (Segala and Cavallaro (2014) and Chocron et al. (2014)). Segala and Cavallaro (2014) used a micro-scale approach to model UHMW-PE composite under blast and ballistic loading. The fibre and matrix were modelled explicitly for two plies, but the scale of the fibres was increased and the overall model dimensions were small due to the size of the computational model. Chocron et al. (2014) used a quasi micro-meso scale approach where the fibres were bundled into strips which were connected to strips of matrix material. These fibre and matrix strips were arranged into a ply and multiple plies were generated to form a laminate. This approach is more computationally tractable, but increases scaling of the fibre bundle strips was still required in the thickness direction to make the model solvable within a reasonable period of time for a moderate target thickness (~11 mm). Although valuable to understand the mechanisms of penetration at the micro- and meso-scale, these approaches are still impractical for modelling thick UHMW-PE composite.

Approaches based on the continuum or macro-scale present a more practical alternative to solve typical engineering problems. However the complexity of the constitutive equations and characterisation tests necessary to describe an anisotropic material at a macro or continuum level increases significantly. Nonetheless, this approach has historically been a more feasible and widely adopted method for modelling UHMW-PE composites under impact. Iannucci et al. (2009) propose a plane stress material model that includes separate matrix and fibre failure
and a polynomial description of the non-linear in-plane shear behaviour. The model showed good global deformation behaviour for thin targets impacted at approximately 350 m/s. Bürger et al. (2012) proposed a progressive failure model to predict the structural response of Dyneema composite. The model included description of fibre failure in tension/compression, inter-fibre failure, and in-plane shear failure. The lack of a delamination model however led to poor predictions of deformation. Grujicic et al. (2008) proposed a multi-scale approach to modelling composite laminates. In this approach, a unit cell model was coupled to a continuum model for which the continuum model was used to update the deformation state of the unit cell, which in turn was used to compute the material stress state. Validations against ballistic experiments from Iremonger (1999) showed good agreement with the target deformation. Krishnan et al. (2010) and Assaf et al. (2013) have also reported methods for modelling UHMW-PE composite using a continuum description, however the constitutive equations were not provided. Other continuum models for composites have also been reported by Gama and Gillespie (2011) and Beissel (2014), although these models have yet to be applied to UHMW-PE composite.

All of the models discussed up to now (including micro, meso and macro scale models) have been validated for cases where the ballistic limit is less than 800 m/s with most within the 300 m/s to 400 m/s range. For this impact regime, the material strength dominates the ballistic resistance. As a result they all assume a linear equation of state (i.e. linear compressibility under shock loading). For thick UHMW-PE composite, the ballistic limit velocity can be significantly higher, introducing higher impact pressures and non-linear compressibility (as discussed in section 2.4.4). Therefore application of these models at higher impact velocities would become increasingly inaccurate. The inclusion of a non-linear EoS is critical in order to capture these non-linear effects at high impact velocities.

The thermodynamic (EoS) response of a material and its ability to carry tensile and shear loads (strength) is typically treated separately within hydrocodes such that the stress tensor can be decomposed into volumetric and deviatoric components (Wilkins and Giroux (1963) and Anderson (1987)). The stress tensor is expressed as:

\[ \sigma_{ij} = s_{ij} + P \delta \]  

(2.36)

where \( \sigma_{ij} \) is the total stress, \( s_{ij} \) is the deviatoric stress component, \( P \) is the pressure and \( \delta_{ij} \) is the Kronecker delta function. Since the mechanical properties of fibre-reinforced composites are anisotropic (at least at the meso- and macro-scale level), the deviatoric and hydrostatic components are coupled. That is deviatoric strains will produce a volumetric dilation and hydrostatic pressure leads to non-uniform strains in the three principal directions (Anderson et al., 1994).

In order to remain within the framework of existing hydrocodes (i.e decomposition of the stress tensor into deviatoric and volumetric components), O’Donoghue et al. (1992) and Anderson et al. (1994) proposed a new constitutive formulation for anisotropic material that couples the deviatoric stress and pressure. In this formulation the deviatoric stress and pressure are expressed as a function of both the volumetric and deviatoric strains, this is discussed in more detail in section 5.1.1.1. Anderson et al. (1994) enhanced the model to include large rotation of elements, failure criteria, and representation in three-dimensional space.

Hiermaier et al. (1999) and Hayhurst et al. (1999) built on the work of Anderson et al. (1994) and incorporated a non-linear equation of state and an orthotropic failure criterion. The model was implemented in a commercial hydrocode (ANSYS® AUTODYN®) and validated against hypervelocity impacts of Nextel and Kevlar® epoxy composite as part of the Advanced Material Model for Hypervelocity Impact Simulation (AMMHIS) project. Simulation results were com-
pared to experiment (16 mm aluminium spheres at 6290 m/s on a bumper shield composed of aluminium, Nextel™ and Kevlar® epoxy plates) with respect to the perforated hole diameter and showed good agreement for Nextel™ but predicted twice the hole diameter for Kevlar®. Tham et al. (2008) used this model to study the ballistic impact of Kevlar® helmets and showed accurate prediction of the ballistic limit in the range of 600 m/s.

Riedel et al. (2003a), Clegg et al. (2006) and Riedel et al. (2006) made improvements to the AMMHIS model to allow for better predictions of damage and residual strength in composites after impact in the Advanced Material Damage Model for Numerical Simulation Codes (ADAMMO) project. In this study they incorporated orthotropic non-linear irreversible hardening based on the quadratic yield function derived in Chen et al. (1997), and failure initiation based on the combined stress in each orthotropic material plane and energy-based softening. This model improved the predicted hole diameter for the Kevlar® epoxy plate while maintaining good agreement for Nextel™.

The ADAMMO model was applied to CFRP by Wicklein et al. (2007) and Wicklein et al. (2008) in the Carbon Fibre Material Models for Hypervelocity Impact Numerical Simulations (CARMHIS) study. This model also incorporated damage coupling in the different material directions. Validation was performed on CFRP honeycomb structures impacted by aluminium spheres between 5000 m/s to 6000 m/s (near ballistic limit) at normal and oblique angles. In these simulations, there was good agreement with the hole diameter as well as the front plate delamination diameter. The rear plate delamination diameter was however under-predicted. Table 2.5 provides a summary of the development of the non-linear orthotropic continuum model.

| Table 2.5: The non-linear orthotropic model development |
|-----------------|-----------------|-----------------|
| Strength Model    | Orthotropic Linear Elastic Compaction Curve | Orthotropic Linear Elastic Plasticity | Orthotropic Linear Elastic Plasticity |
| Failure Model     | Tensile Failure Stresses Tensile Failure Strain | Tensile Failure Stresses Maximum Shear Stress Damage Description | Tensile Failure Stresses Maximum Shear Stress Damage Description |
|                   |                 |                 | Damage Coupling Coefficient Restriction on Plasticity Parameter |
|                   |                 |                 | Orthotropic Post Failure |

A number of researchers have applied the non-linear orthotropic model for UHMW-PE composites with varying levels of success (Hayhurst et al. (2000), Herlaar et al. (2005), Ong et al. (2011), Heisserer and Van der Werff (2012) and Lässig et al. (2015)). Ong et al. (2011) assumed material properties of UHMW-PE composite based on those of Kevlar® with some data from literature, which resulted in poor predictions of the penetration behaviour. Hayhurst et al. (2000), Herlaar et al. (2005) and Heisserer and Van der Werff (2012) used material input parameters derived from a range of experiments, and reported better prediction, although the results cannot be independently verified because the material parameters are not provided. Lässig et al. (2015) conducted extensive experimental characterisation of UHMW-PE composite, the results of which were used to produce a material data set for UHMW-PE composite for the non-linear orthotropic material model. The model was validated against 15 mm thick UHMW-PE composite impacted by 6 mm aluminium spheres between 2000 m/s to 6600 m/s. The model gave good predictions of the residual and ballistic limit velocity for this impact condition.
The vast majority of applications using the non-linear orthotropic model have been for hypervelocity impact greater than 2000 m/s. For this impact condition, the model has been reasonably successful in replicating the key physical phenomena for Nextel™, AFRP, CFRP and UHMW-PE composite. There have been very few studies on the model for impact velocity below 2000 m/s, particularly for UHMW-PE composite. For this impact velocity range, the material strength description becomes increasingly important in determining the ballistic resistance of the material. Nonetheless, the recent results in Lässig et al. (2015) are promising and provide a good foundation for further investigations on the suitability of this model for thick UHMW-PE composite under ballistic impact.

2.5.3 Evaluation of Non-Linear Orthotropic Material Model

In this section the non-linear orthotropic model and the material data set derived in Lässig et al. (2015) will be evaluated under ballistic impact. This evaluation will inform improvements to the non-linear orthotropic model for this thesis.

The model is evaluated against experimental ballistic limit results reported in Heisserer and Van der Werff (2012). In this work, the ballistic limit velocity ($V_{50}$) of UHMW-PE composite was determined against 5.56 mm and 20 mm FSPs for targets between 2.6 mm to 50.6 mm thick. For this evaluation study, the ballistic limit performance predictions of the model will be assessed against the larger 20 mm FSP for three target thicknesses. Heisserer and Van der Werff (2012) reported a $V_{50}$ of 777 m/s, 975 m/s and 1099 m/s for Dyneema® HB26 targets of 31 mm, 40.8 mm and 50.6 mm thick, respectively, against the 20 mm FSP.

The numerical model is designed and meshed in ANSYS® Workbench and ANSYS® AUTO-DYN®. In AUTODYN® the through-thickness z-direction is designated the 11 direction, and the in-plane x-y plane are the 22 and 33 directions respectively. The projectile and target are meshed using 8-node hexahedral elements with a single integration point. The projectile was meshed with 9 elements across the projectile diameter. The same mesh size is applied to the target in the penetration region. The size is then graded towards the target edge to reduce the size of the numerical problem. No boundary conditions were applied to the target and the projectile was imposed with an initial impact velocity. A more thorough discussion of the model mesh and boundary conditions is detailed in Chapter 5. The projectile was modelled using the Steel S-7 material model in the AUTODYN® library, which uses a linear EoS and the Johnson-Cook strength model (Johnson and Cook, 1983). For each target, several impact velocities were simulated so that at least one partial penetration and several complete penetration results were obtained. These impact-residual velocity results were fitted to a Lambert-Jonas equation (Lambert and Jonas, 1976):

$$V_R = a (V_I^p - V_{BL}^p)^{1/p}$$  \hspace{1cm} (2.37)

where $V_R$ is the residual velocity, $V_I$ is the impact velocity and $a$ and $p$ are curve fit parameters. $V_{BL}$ is the ballistic limit velocity and is compared to the experimental $V_{50}$. Figure 2.19(a) shows the impact-residual velocity results from the numerical simulation using the non-linear orthotropic model, with the data points fitted to the Lambert-Jonas equation. The ballistic limit predictions from the numerical model, $V_{BL}$, are compared to experimental $V_{50}$ values in Figure 2.19(b) and Table 2.6. The results show the predicted ballistic limit velocity is between 30% to 40% lower than the experimental value for this impact condition.
Figure 2.19: (a) Impact-residual velocity results, Lambert-Jonas parameter in brackets \((a, p, V_{BL})\). (b) Experimental and numerical ballistic limit velocity comparison

Table 2.6: Ballistic limit velocity comparison

<table>
<thead>
<tr>
<th>Thickness (mm)</th>
<th>Experiment (V_{50}) (m/s)</th>
<th>Numerical (V_{BL}) (m/s)</th>
<th>Difference (%)</th>
</tr>
</thead>
<tbody>
<tr>
<td>31</td>
<td>777</td>
<td>490</td>
<td>37</td>
</tr>
<tr>
<td>40.8</td>
<td>975</td>
<td>677</td>
<td>31</td>
</tr>
<tr>
<td>50.6</td>
<td>1099</td>
<td>779</td>
<td>29</td>
</tr>
</tbody>
</table>

For this velocity range, the material response is driven more by the strength model as opposed to the equation of state in Lässig et al. (2015) (hypervelocity). As such the disagreement observed in this benchmark study does not contradict the high level of agreement observed for hypervelocity impact conditions in Lässig et al. (2015). The difference between the experimental and numerical prediction of the ballistic limit is therefore believed to be driven by deficiencies in the strength and failure model for this velocity regime.

The strength and failure model was investigated by modelling single elements under normal and shear stresses. It was found that under through-thickness shear stress, the element would fail prematurely below the specified through-thickness shear failure stress. It was found that if the through-thickness tensile strength, \(S_{zz}\), was increased, failure in through-thickness shear was delayed. These observations are shown in Figure 2.20 where incremental increases in the through-thickness tensile strength \(S_{zz}\), leads to an increase in the through-thickness shear failure stress, \(\sigma_{xz}\) or \(\sigma_{yz}\).
Figure 2.20 clearly shows coupling between the through-thickness failure properties leading to premature failure in the out-of-plane shear direction for materials with reduced tensile strength. The combined stress failure model used in the non-linear orthotropic model applies the following failure initiation criterion (Clegg et al., 2006):

\[
\left( \frac{\sigma_{ii}}{S_{ii} (1 - D_{ii})} \right)^2 + \left( \frac{\sigma_{ij}}{S_{ij} (1 - D_{ij})} \right)^2 + \left( \frac{\sigma_{ki}}{S_{ki} (1 - D_{ki})} \right)^2 \geq 1 \quad \text{for } i, j, k = 1, 2, 3 \quad (2.38)
\]

where \( \sigma \) is the stress, \( S \) is the strength and \( D \) is the damage in each of the material directions. According to this failure model, when an element fails in one particular direction (i.e. \( zz \) direction), damage begins to accumulate in the other directions (i.e. \( xz \) and \( yz \)). Since there is such a large discrepancy between the through-thickness tensile and shear strengths of UHMW-PE composite (Lässig et al. (2015) measured 1 MPa in tension and more than 25 MPa in shear (specimen did not fail in this test)), coupling of the two properties (Equation 2.38) leads to premature failure. This is believed to be one of the primary reasons why the numerical model under-predicts the penetration resistance of the target.

Inspection of the model shows significant through-thickness tension or delamination failure as shown in Figure 2.21.

Figure 2.20: The effect of varying through-thickness tensile strength on the shear stress-strain results from single element simulation
Figure 2.21: 50.6 mm UHMW-PE composite target at 250 μs after initial impact by 20 mm FSP at 1000 m/s. (a) full target, (c) cross-section of target and (b) cross section of target with through-thickness tension damage contour (red - fully damaged, blue - undamaged).

Whilst these elements have failed in the through-thickness direction (interlaminar failure), the in-plane properties of these elements should still remain, i.e. the fibres can still carry in-plane loads. However, through-thickness failure of these elements leads to large expansion of the element in the thickness direction. The strain in this direction therefore increases, making the element susceptible to the strain-based erosion model. Here elements are deleted at 150% of the effective geometric strain, which is defined in the material direction as (ANSYS, 2013a):\[
\varepsilon_{eff} = \frac{2}{3} \left[ (\varepsilon_{11}^2 + \varepsilon_{22}^2 + \varepsilon_{33}^2) - (\varepsilon_{11} \varepsilon_{22} + \varepsilon_{22} \varepsilon_{33} + \varepsilon_{33} \varepsilon_{11}) + 3 \left( \varepsilon_{12}^2 + \varepsilon_{23}^2 + \varepsilon_{31}^2 \right) \right]^{1/2}
\] (2.39) or in the principal direction as:\[
\varepsilon_{eff} = \frac{2}{3} \left[ (\varepsilon_{11}^2 + \varepsilon_{22}^2 + \varepsilon_{33}^2) - (\varepsilon_{11} \varepsilon_{22} + \varepsilon_{22} \varepsilon_{33} + \varepsilon_{33} \varepsilon_{11}) \right]^{1/2}
\] (2.40)

An undesirable effect of this is that elements that have failed in the through-thickness direction but can still carry in-plane loads are deleted due to excessive strain in the thickness direction. As a consequence a key mode of penetration resistance (in-plane tension) is removed prematurely and the ballistic performance is under-predicted. Another non-physical effect is also observed for elements that have failed in the thickness direction but have not reached the effective strain for erosion. In this case the element volume increases significantly from stretching in the thickness direction (see elements around the penetration zone in Figure 2.21 (b) and (c)). This increases the surface area of the element, artificially preventing failure in the in-plane directions, as forces in these directions lead to lower stresses due to the increased area.

A typical solution to avoid premature deletion of elements is to increase the effective strain limit whereby element erosion takes place (by delaying erosion in elements that only fail in the through-thickness direction), however this leads to elements with very large volumes with artificially higher in-plane failure loads. Therefore erosion models based on effective or any other formulations based on “smeared” or “average” strains are not suitable for anisotropic materials.
This evaluation study shows the importance of the strength, failure and erosion models for predicting performance in the ballistic regime. The study clearly shows that there are deficiencies in the current failure and erosion descriptions that lead to poor predictions for the impact regime of interest. Most noticeably the model exhibits premature through-thickness shear failure in the UHMW-PE composite due to coupling of the very weak through-thickness tensile strength to the much higher through-thickness shear strength in the failure initiation criterion (Equation 2.38). Furthermore element erosion based on a “smeared” effective strain was found to be unsuitable for anisotropic materials due to directional failure that prevents the resistance in the other material principal directions from being realised. Both these problems act to reduce the overall penetration resistance of the target, leading to under-prediction of the ballistic performance. These problems must be addressed in order to improve the accuracy of the non-linear orthotropic model for UHMW-PE composite under ballistic impact.

2.6 Summary

A literature review has been conducted that comprehensively and critically appraises all areas relevant to the ballistic performance of UHMW-PE composite. In this review the ballistic properties of UHMW-PE fibres, yarns and fabrics, and the underlying transverse impact theories were summarised. The response of thin and thick UHMW-PE composite to ballistic impact was reviewed, with thick composites defined by penetration and failure in multiple stages. The material properties important to the ballistic performance were also reviewed, covering the material tensile, shear, compression and shock properties. Finally, analysis tools such as analytical and numerical models were reviewed and evaluated for UHMW-PE composite through a benchmarking exercise of the current state-of-the-art. From this review, several scientific gaps were identified that provide scope for the work in this thesis, which is summarised below.

Ballistic performance of thick UHMW-PE composite

The ballistic properties of UHMW-PE fibre, yarn and strips (single ply) impacted transversely are well understood using classical yarn impact theory. The performance of thin UHMW-PE composite is also known to be driven predominantly by membrane loading and fibre tensile failure, with extensive literature published in this area. In contrast, there is limited understanding of the response of thick UHMW-PE composite to ballistic impact. The ballistic limit performance of UHMW-PE composite has been reported for target thicknesses up to 50 mm, although these studies did not investigate the penetration and failure mechanisms. Analysis of the damage mechanisms have been performed on 40 mm thick (two 20 mm targets clamped together) UHMW-PE composite targets, however this study only characterised the different types of intra- and inter-laminar damages modes (e.g. fibre-matrix failure and delamination modes) exhibited by UHMW-PE composite and not the fibre failure mode. This is crucial as the fibres are primarily responsible for the load-carrying and energy absorption under ballistic impact. Some studies have identified that thick UHMW-PE composite exhibits an initial localised penetration followed by global, membrane-like deformation of the rear face. These penetration modes are responsible for different levels of energy absorption under impact and therefore determine the performance of the material. However, transition from one penetration mode to another has not been characterised and the mechanisms driving transition are not well understood.
Analytical models for thick UHMW-PE composite

Analytical models provide significant insight into the governing penetration and failure mechanisms when validated by experimental results. The deformation and failure of fibres, yarns and composite strips for instance can be accurately predicted using classical yarn impact theory. In this literature review, two existing fabric models were applied to UHMW-PE composite and both were shown to be accurate in predicting the ballistic performance of thin targets. This shows that thin UHMW-PE composite can be described using membrane theory. However, these models deviate from the experimental ballistic limit results when applied to thicker targets. No analytical models currently exist to describe the penetration of thick UHMW-PE composite. A new model is required that takes into account the different penetration mechanisms of thick targets, providing insight into the governing penetration mechanics and allowing prediction of ballistic performance of thick targets.

Numerical models for UHMW-PE composite

Experimental characterisation of the ballistic performance of UHMW-PE composite can be prohibitively expensive, so it is highly desirable to establish computationally efficient numerical models that accurately predict the ballistic response of the material. Recent advances (Chocron et al., 2014) have proposed an accurate modelling strategy using a quasi-micro-meso scale discretisation approach to analyse UHMW-PE composite under ballistic impact. However for thick targets, discretisation at such small scales leads to models that are computationally expensive and inefficient. Multi-scale and continuum approaches to modelling UHMW-PE composite have been proposed. However, only limited validation of these models has been performed for impact velocities below 900 m/s and no validation has been conducted for higher impact velocities. At higher impact velocities, a non-linear EoS is required to account for the non-linear shock compressibility of the material. The non-linear orthotropic continuum model accounts for non-linear shock compressibility, although this model has only been validated for impact velocities between 2000 m/s to 6600 m/s.

An evaluation of this model was performed in this literature review at impact velocities below 2000 m/s which showed the model as it currently stands can not capture the response of UHMW-PE composite under ballistic impact and provides poor predictions of performance. This is because the model couples the through-thickness shear and through-thickness tensile failure, leading to premature failure of one of the modes due to the significant differences in strength in the two directions. An effective strain element erosion model is also currently used which does not appropriately account for the properties of the composite in different directions. Currently no numerical model strategy has been shown to predict the ballistic performance of thick UHMW-PE composite targets impacted between 900 m/s to 2000 m/s. This is an important velocity regime, reflecting many existing and emerging ballistic threats for armoured vehicles.
Chapter 3

Experimental Assessment of the Ballistic Impact of Thick UHMW-PE Composite

The response of thick UHMW-PE composite under ballistic impact is experimentally investigated in this chapter. This investigations aims to address the gaps that were highlighted in the literature review, namely:

- The key penetration and failure mechanisms of thick UHMW-PE composite is not well understood, particularly the failure mode of the load-bearing fibres and how this changes with thickness and penetration depth.
- Although thick UHMW-PE composite has been identified to exhibit multiple stages of penetration, the transition between different penetration and failure stages is still poorly understood.
- The ballistic performance of thick UHMW-PE composite and its efficiency compared to traditional armour materials.

To address this, an extensive experimental research program was conducted. Depth of penetration tests on semi-infinite targets and ballistic limit tests on targets of finite thickness are performed for a wide range of thickness against FSPs. These projectiles are chosen as they are standardised surrogates of artillery round fragments. Furthermore the results can be used with existing data already published in literature for thinner UHMW-PE composite. DoP results are reported for impact velocities up to 1300 m/s and V_{50} results reported for targets up to 100 mm thick. The target response was investigated in-situ using high speed photography of the impact event. Post-test analyses of the targets were performed visually as well as with SEM to study the penetration and failure mechanisms.

The work in this chapter has been published in the following peer-reviewed journal publications:


3.1 Material

The experimental work reported in this chapter is performed using a grade of UHMW-PE composite, Dyneema® HB26, produced by DSM Dyneema®. This composite consists of approximately 80 percent by weight Dyneema® SK76 fibres and a polyurethane matrix (polyetherdiol-aliphatic diisocyanate polyurethane (Karthikeyan et al., 2013b)). Dyneema® HB26 consist of uni-directional plies, with a [0/90]n stacking sequence. The laminates tested in this work were consolidated under a pressure and temperature of 14 MPa and 125°C respectively (the manufacturer did not specify a consolidation time). Micrographs of the cross-section show a circular fibre with a diameter of approximately 17 µm and an average single ply thickness of approximately 70 µm (Figure 3.1).

![Figure 3.1: Cross-section of Dyneema® HB26 composite](image)

3.2 Ballistic Impact on Semi-infinite Targets

3.2.1 Methodology

Depth of penetration tests are performed for several impact velocities on Dyneema® HB26 panels measuring 150 mm × 150 mm laterally. The target thickness varied with impact velocity and was determined so that the target thickness was more than double the estimated DoP to limit any influence of the target backing. These panels are either 100 mm or 150 mm thick and are produced by secondary consolidation of 50 mm thick laminates at 0.9 MPa and 125°C. The panels are clamped to a 100 mm thick steel backing to avoid back face bulging, and impacted at the centre with MIL-DTL-46593B (Department of Defense, 2006) 20 mm calibre FSPs. The geometry of the projectile is shown in Figure 3.2 and the target setup in Figure 3.3.
The depth of penetration test on semi-infinite targets were performed on two different ranges, a summary of the test is shown in Table 3.1.

- Test 1 to 2 shown in Table 3.1 were performed using a 20 mm barrel with a powder breach. The targets were positioned 10 m from the muzzle and projectile velocity measurements were made via dual light-sensing skyscreens (1 m apart with a midpoint 2.5 m from the target) and confirmed via radar measurements.

- Test 3 to 4 were performed with projectiles fired from a universal receiver mounted to a 20 mm barrel at a different ballistics facility. For this setup, the target were mounted to a frame 25 m from the gun muzzle. The projectile velocity was measured via two laser break screens with a midpoint 2 m from the target.

The penetration depth was measured as the difference between the original target thickness and the remaining unperforated thickness. The remaining unperforated thickness of the target was determined by peeling away the perforated layers until no more perforations was visible and the thickness measured. The impacted targets were investigated by visual inspection as well as SEM, to investigate the failure mode of the load-bearing fibres at the penetration cavity. This
allows investigation of the evolution of fibre failure as the projectile penetrated through the laminate.

The specimens for the SEM were prepared by peeling off fibres around the penetration cavity at different penetration depths. These specimens were mounted to a stub with carbon tape and sputter coated with gold to cover the specimen in a thin layer of electrically conductive material. The samples were viewed using a scanning electron microscope under high vacuum mode.

### 3.2.2 Results and Discussion

DoP test results are summarised in Table 3.1 and Figure 3.4. Depth of penetration performance is linear with increasing impact velocity and suggests the penetration mechanisms is the same for the range of impact velocities investigated. A linear relationship was also observed in the DoP test performed by Heisserer et al. (2013) who impacted steel spheres at lower velocities into 25 mm thick UHMW-PE composite without any backing support.

<table>
<thead>
<tr>
<th>Test #</th>
<th>Thickness (mm)</th>
<th>Impact Velocity (m/s)</th>
<th>DoP (mm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>100.8</td>
<td>815.0</td>
<td>30.0</td>
</tr>
<tr>
<td>2</td>
<td>150.8</td>
<td>1304.6</td>
<td>61.4</td>
</tr>
<tr>
<td>3</td>
<td>151.2</td>
<td>990.6</td>
<td>39.8</td>
</tr>
<tr>
<td>4</td>
<td>151.2</td>
<td>1037.8</td>
<td>45.2</td>
</tr>
</tbody>
</table>

![Figure 3.4: Depth of penetration of semi-infinite UHMW-PE composite targets against 20 mm FSP](image)

Visual inspection of the target showed extensive delamination failure through-the-thickness of the target (Figure 3.5), particularly through the perforated layers. Delaminations below the
penetration depth are also visible and occur throughout the panel, as shown in figure 3.5. For delamination formed below the penetration depth, most are near the penetration cavity with some extending all the way to the edge of the target.

Although delamination is not a significant energy absorbing damage mode for UHMW-PE composite due to the low interlaminar properties, the existence of delamination planes beyond the penetration depth of semi-infinite targets is important for finite thickness targets where transition to bulging deformation occurs. This is because the existence of delamination plane reduces the bending stiffness of the remaining unperforated material, and allows the target back face to undergo large bulge deformation when the pressure wave reaches the back and is released (causing a velocity jump). For fibre-reinforced composites with higher resistance to delamination (e.g carbon fibre-epoxy composite) or homogenous materials that do not exhibit delamination failure (e.g metals or ceramics), as the pressure wave is released on the back of the target, the velocity jump is resisted through bending (Woodward and Cimpoeru, 1998) and so the target does not exhibit bulge deformation to the same degree as UHMW-PE composite.

Figure 3.6 shows micrographs of fibres taken from various locations along the penetration cavity. Figure 3.6(a) shows a fibre on the target surface with significant compression in the through-thickness direction. The cross-section of the fibre along the fracture plane is shown and indicates fibre shearing failure. Figure 3.6(b) shows a fibre from the top view, where the expansion in the in-plane directions as a result of through-thickness compression can be seen. Figure 3.6(c) and 3.6(d) show images from the side view, which demonstrate the shear failure of the fibres.
Fibre shear failure was dominant at all locations through the circumference of the penetration cavity for these DoP targets. This failure mode is characteristic of a shear plugging mechanism, where failure occurs around the perimeter of the blunt projectile. The fibres did not show failure in through-thickness compression however, because the micro-fibrillar microstructure allows the fibres to undergo significant through-thickness strain under compression as shown in Figure 3.6(a) and (b). In this investigation, the prevalence of through-thickness fibre compression diminished with increasing penetration depth, as shown in Figure 3.6(c) and (d). This is because as the projectile penetrates through the target, the projectile velocity decays, reducing the impact pressure and therefore fibre through-thickness compression.
3.3 Ballistic Performance of Finite Thickness Targets

3.3.1 Methodology

The ballistic limit tests were performed on Dyneema® HB26, with target thicknesses between 10 mm to 100 mm. Panels up to a thickness of 50 mm were obtained from the manufacturer, consolidated under the conditions specified in section 3.1 (pressure and temperature of 14 MPa and 125°C). Not all 75 mm and 100 mm thick UHMW-PE panels could be sourced as a monolithic panel, so some additional panels of these thicknesses were produced through a secondary consolidation process using thinner panels (25 mm and 50 mm) at 0.9 MPa and 125°C. Comparison between monolithic panels and those produced through the secondary consolidation process showed no discernible performance difference, although the panels typically separated along these new consolidated planes under impact. The panels had lateral dimensions of 300 mm × 300 mm, except for the 75 mm and 100 mm thick targets which were 400 mm × 400 mm. During testing it was observed that thicker targets exhibit larger bulge deformation before failure. Therefore wider targets were used for the thickest targets to ensure sufficient lateral dimension for the bulge to develop prior to perforation at the ballistic limit.

Ballistic limit tests were performed for the range of panel thicknesses with MIL-DTL-46593B spec 12.7 mm and 20 mm calibre FSPs with a mass of 13.4g and 53.8g respectively (Department of Defense, 2006). Figure 3.7 compares the geometry of the two projectiles.

![Figure 3.7: Geometry of MIL-DTL-46593 12.7 mm and 20 mm calibre fragment simulating projectiles (dimensions in mm) (Department of Defense, 2006)](image)

Testing was conducted using a range of facilities according to the projectile calibre and required impact velocity:

- For the lower impact velocities, typically less than 1000 m/s, tests were performed on 12.7 mm and 20 mm barrels mounted to a universal receiver. Velocity measurements were made via two laser break screens with a mid-point 2.5 m from the target.
- For intermediate velocities, typically between 1000 m/s to 1300 m/s, tests were performed using 12.7 mm and 20 mm diameter barrels with a powder breech. The target was mounted to a frame 10 m from the gun muzzle and projectile velocity measurements were made via optical chronograph with a mid-point 3 m from the target;
- For the highest velocities, between 1300 m/s to 2100 m/s, tests were performed using 20
mm and 30 mm diameter barrels with a powder breech, in which the sub-calibre FSPs were saboted. For these tests flat-backed variants of the FSPs of the same mass were used. Velocity measurements were made via dual light-sensing skyscreens (1 m apart and mid-point 2 m from the target) and confirmed via radar measurement.

A 50th percentile probability of perforation was used to define the ballistic limit velocity, \( V_{50} \). Calculation of the \( V_{50} \) was determined from an even distribution of partial penetration and complete penetration results, as per MIL-STD-662F (Department of Defense, 1997). No witness plate was used for the UHMW-PE tests; rather complete penetration was defined by perforation of the rearmost ply of the composite panel. Test on panels 25 mm thick or less were initially performed with the panels bolted between two 8 mm thick steel plates with four 100 mm diameter apertures as shown in Figure 3.8. This was for economy of target materials, allowing four tests to be performed on the one target. To ensure the \( V_{50} \) results were not influenced by the additional confinement of the steel plates, verifications tests were performed with a single shot on a full panel. Results on the confined targets were within the experimental scatter range of the single shot verification tests, consistent with findings from Lee et al. (1994). Targets thicker than 25 mm were not tested using the aperture confinement as excessive drawing of material was expected to influence the result.

Figure 3.8: UHMW-PE composite panel sandwiched between two steel plates with four 100 mm apertures, (right) front and back view, (left) side view
A grid pattern of 20 mm x 20 mm squares was marked onto the front and back face of the targets. The grid on the front face was used to determine the degree of in-plane deformation from distortion in the grid on the front face while the back face grid was used to track the development of the back face bulge. A Photron SA5 high speed camera was used to record the impact event. Images were taken every 50 μs with a 12.5 μs exposure. The camera was positioned with a side view of the target as shown in Figure 3.9. A mirror positioned at an angle to the target back face was kept in frame, providing a side and perspective view of the back face in a single frame. The image was calibrated based on the approximate line-of-flight of the projectile. Variability in the line-of-flight and resolution resulted in a measurement error of approximately ±3 mm. The side view was used to track the bulge apex and hinge radius and the perspective view of the back face was used to confirm the bulge radius. Comparison showed good agreement between the side view and perspective view measurements within the error margin. A summary of the ballistic limit test configurations and results is given in Table 3.2, where \( \sigma \) is the standard deviation and \( c_v \) is the coefficient of variation.

### Table 3.2: Ballistic limit test results

<table>
<thead>
<tr>
<th>Thickness (mm)</th>
<th>Projectile</th>
<th>No. of Test</th>
<th>No. used for ( V_{50} )</th>
<th>( V_{50} ) (m/s)</th>
<th>( \sigma ) (m/s)</th>
<th>( c_v ) (%)</th>
</tr>
</thead>
<tbody>
<tr>
<td>9.1</td>
<td>12.7 mm FSP</td>
<td>11</td>
<td>4</td>
<td>506.0</td>
<td>26.4</td>
<td>5.2</td>
</tr>
<tr>
<td>20</td>
<td>12.7 mm FSP</td>
<td>11</td>
<td>6</td>
<td>825.8</td>
<td>17.2</td>
<td>2.1</td>
</tr>
<tr>
<td>25.2</td>
<td>12.7 mm FSP</td>
<td>9</td>
<td>4</td>
<td>1021.4</td>
<td>8.5</td>
<td>0.0</td>
</tr>
<tr>
<td>35.1</td>
<td>12.7 mm FSP</td>
<td>6</td>
<td>6</td>
<td>1250.3</td>
<td>36.1</td>
<td>2.9</td>
</tr>
<tr>
<td>50.4</td>
<td>12.7 mm FSP</td>
<td>5</td>
<td>2*</td>
<td>1656.5</td>
<td>16.3</td>
<td>1.0</td>
</tr>
<tr>
<td>10</td>
<td>20 mm FSP</td>
<td>6</td>
<td>4</td>
<td>393.9</td>
<td>43.0</td>
<td>10.9</td>
</tr>
<tr>
<td>20</td>
<td>20 mm FSP</td>
<td>9</td>
<td>6</td>
<td>620.1</td>
<td>19.6</td>
<td>3.2</td>
</tr>
<tr>
<td>36.2</td>
<td>20 mm FSP</td>
<td>12</td>
<td>4</td>
<td>901.4</td>
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<td>1.1</td>
</tr>
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<td>20 mm FSP</td>
<td>6</td>
<td>4</td>
<td>2001.8</td>
<td>91.8</td>
<td>4.6</td>
</tr>
</tbody>
</table>

*Calculated from a single partial penetration and complete penetration result, does not meet the minimum requirement of MIL-STD-662*
3.3.2 Results and Discussion

The ballistic limit tests results are shown in Figure 3.10 in terms of the target thickness and non-dimensional areal density, $AD_tA_p/m_p$. As shown in Figure 3.10(a), the ballistic limit of UHMW-PE composite against both 12.7 mm and 20 mm FSP increases linearly with increasing target thickness. When the results are plotted in terms of the non-dimensional areal density in Figure 3.10(b), the $V_{50}$ values for the two calibre projectiles collapse onto the same curve, indicating the performance of UHMW-PE composite is independent of projectile geometric scaling for this class of projectile. This scaling was also observed from results compiled for the 5.56 mm and 20 mm FSP in the literature review, and clearly show that UHMW-PE composite exhibits the same penetration and failure mechanism independent of projectile scale. The results are reported for $AD_tA_p/m_p$ close to 0.6, which is two times higher than what has previously been reported in literature. The results shows that the performance does not taper off for $AD_tA_p/m_p$ beyond 0.3 and instead the ballistic performance remains linear well past $AD_tA_p/m_p$ of 0.3 and beyond 0.6 from these test results. Since kinetic energy is proportional to the square of the velocity ($mV^2/2$), a linear increase in ballistic performance ($V_{50}$) with increased target thickness or $AD_tA_p/m_p$ suggest a non-linear increase in energy absorbed with increased target thickness (for the range of target thickness investigated in this work).

![Figure 3.10](image)

**Figure 3.10:** Dyneema® HB26 ballistic limit velocity results against 12.7 mm and 20 mm FSP in terms of (a) target thickness and (b) non-dimensional areal density

3.3.2.1 Penetration and Failure Mechanisms

Figure 3.11 shows the front face of a target, where some damage mechanisms can be seen. Fibre fracture was seen at the penetration cavity and delaminations were seen throughout the laminate. Other damage mechanisms seen included fibre-matrix debonding (seen in Figure 3.11 as stray mostly-detached fibres) and ply splitting (seen in Figure 3.11 as separations in the ply that disrupt the front face grid). These damage mechanisms have been characterised in other research (Greenhalgh et al., 2013). Composite melting was particularly prevalent around the penetration cavity at the front of the target, which has been attributed to shock-induced heating (Chocron et al. (2013) and Greenhalgh et al. (2013)). As shown in Figure 3.11, the grid on the target front face displayed little permanent deformation away from the penetration site, indicating that the load distribution is localised around the penetration zone. In this region, substantial elastic recoil of the fibres was observed in high speed video which was caused by elastic recovery of the loaded fibres upon fibre fracture.
High speed images of the target back show bulge formation occurred in the shape of a pyramid, which is shown in Figure 3.12. This is characteristic of a cross-ply or orthotropic laminate (Tan and Khoo, 2005) or cross-woven fabric (Chocron et al., 2010) because the impact of a projectile engages the horizontal and vertical strip of fibres in contact, pushing them forward and forming the pyramid edges. Figure 3.12 shows the significant in-plane shear deformation that occurred during bulging, which can be seen from the highly distorted grid regions along the diagonal of the bulge due to the pyramid shape. The large amount of drawing in of the material from the edges can also be seen. This drawing in is caused by the extensive deflection and the occurrence of delamination that extends to the edge of the target.
Thin laminates (less than 10 mm in this work) displayed only bulging, while targets 10 mm thick or greater showed a two-stage penetration process. A schematic of the two-stage penetration process as seen in thick targets in the ballistic limit tests is shown in Figure 3.13. The target initially undergoes penetration by shear plugging, shown primarily in Figure 3.13(b), where there was little deflection of the target, followed by bulging or breakout of a sub-laminate, with large deflection and drawing in of material from the panel edges, shown in Figure 3.13(c).

Figure 3.12: High-speed images of a 36 mm thick target impacted by a 20 mm FSP, (a) 200 μs and (b) 450 μs from initial impact
Delamination played a key role in the ballistic response of thick specimens, and is illustrated in Figure 3.13. Due to the very low interlaminar stiffness and strength of UHMW-PE composite, the occurrence of delamination does not contribute much to energy absorption. However for thick laminates, the presence of delamination ahead of the projectile allows large permanent deformation to occur by reducing the bending resistance. Delamination initially occurred in the shear plugging stage (Figure 3.13(a)) due to the propagation of the pressure wave and interaction of release waves, which was also observed in the DoP tests (section 3.2). As the penetration progresses, one of these delaminations form into a transition plane as the pressure wave reaches the back of the target and is released (Figure 3.13(b)), leading to a velocity jump on the back surface of the remaining laminate and dividing the laminate into sub-laminates undergoing either shear plugging or bulging (Figure 3.13(c)).

During bulging, further mode II delaminations form in the bulging sub-laminate due to the shear stresses developed from deformation and bending of the rear portion of the laminate. Delaminations also propagated due to the mode I opening stresses generated from the penetrating impactor. These aspects are illustrated using SEM images of the delaminated surfaces in Figure 3.13, where Figure 3.13(e) and Figure 3.13(f) show fracture surfaces characteristic of mode I and mode II delamination growth. In many cases, the delaminations extended to the edge of the target, and often to all four edges. This resulted in some sub-laminates separating away and breaking the target into multiple pieces. However, in completely perforated targets it was observed that mild fibre bridging and some small intact regions within each delaminated interface meant that the target remained mostly in one piece. For panels of 10 mm thickness
and less, the entire panel underwent bulging (i.e. there was no transition plane).

Fibre fracture was characterised using SEM micrographs of fibre tips taken from the impact site through the target thickness, and is summarised in Figure 3.14. During the shear plugging stage (Figure 3.14(a) and (b)), the fibre fracture surfaces display predominantly fibre cutting or shearing as a result of the sharp edges of the FSP, and is caused by the thick targets resisting bending in the same way as semi-infinite targets.

![Figure 3.14: Fibre fracture morphology from ballistic limit tests (35 mm thick target impacted by 12.7 mm FSP at 1346 m/s). Images taken (a) at the front face, (b) 9 mm from the front face, (c) 18 mm from the front face, and (d) at the back face](image)

There were limited fibres displaying tensile failure in the section of the target undergoing shear plugging. However, there was significant fibre compression in the thickness direction, as seen in the flattened fibres shown in Figure 3.14(a) and 3.14(b). The transverse compressive modulus of micro-fibrillar fibres has been shown to be approximately two orders of magnitude lower than the longitudinal modulus (Phoenix and Skelton (1974) and Cheng et al. (2004)). As such, fibre tensile and compressive failures are considered of secondary importance for energy absorption, and fibre shear failure is the dominant energy absorption mechanism in the shear plugging stage.

During the bulging stage, the prevalence of fibre transverse shear failure decreases while fibre tension failure increases. This is due to large deflection of the bulge, which creates high tensile stresses as the material stretches. Figure 3.14(c) and Figure 3.14(d) show that fibres on the back face of a ballistic limit target exhibited predominantly ductile failure, and had a very different character to the fibres failing in shear on the front face. Unlike fibres failing in shear
that exhibit a clean fracture surface, fibres failing in tension undergo significant elongation and reduction in diameter. This is caused by the highly orientated molecular chains and fibrillar nature of the material, which when coupled with the weak chemical bonds allow slipping under tension (Berger et al., 2003), as described in the literature review.

The observations of ductile tensile fibre failure are in contrast to Koh et al. (2008), who observed predominantly brittle cleavage fibre fracture morphologies in high strain rate (400 s\(^{-1}\) to 850 s\(^{-1}\)) tensile tests of UHMW-PE composite. Under tensile loads, UHMW-PE fibres only fail in a ductile manner due to slow loading rates (i.e. creep or quasi static loading) where there is time for the molecular chains to move or under elevated temperatures (beyond 20° C according to Dijkstra et al. (1989)) where the mobility of the molecular chain is increased, as covered in the literature review. Under ballistic impact the strain rates are significantly higher than creep or quasi static loading, so strain rates effects can not be the reason for the observed ductile failure of the fibres. As such the observed ductile failure of fibres in the bulging portion of the target can only arise from an increase in thermal load due to ballistic impact. Increase in thermal load from ballistic impact occurs upon the release in the shock pressure due to the irreversible shock process (Meyers, 1994). Note that thermal loads can also arise from material plastic deformation, which increases under high strain rate due to a decrease in the thermal diffusion distance (Meyers, 1994). However, Koh et al. (2008) found that this effect was minimal for UHMW-PE composite even at strain rates of 850 s\(^{-1}\).

### 3.3.2.2 Transition of Penetration Modes

Transition between the two penetration stages is a complex phenomenon. It has been proposed that transition is due to delamination failure of the laminate target induced by shear dominated stresses in bending (Greenhalgh et al., 2013). However, the target undergoing shear plugging is not undergoing bending. Further, as the thickness increases, the resistance to bending increases with a cubic relationship. This suggests a dependence on thickness, with transition occurring once the sub-laminate underneath the impactor becomes thin enough to undergo large-strain deflection. On the other hand, the occurrence of bulging is dependent on the time taken for the pressure wave to reach the back surface and is related to the transfer of momentum to the bulging sub-laminate. These aspects imply a dependence on the material wave speed and impact velocity. The dependence of transition on the propagation of the stress wave through the material makes it difficult to identify the mechanisms underpinning transition in UHMW-PE composite experimentally. For this reason, the mechanisms for transition will be further investigated numerically in Chapter 5. A numerical approach allows direct observation of the stress wave as it propagates through the material, providing more insight into the effect it has on transition and bulging.

Although complex to characterise, the transition from shear plugging to the bulging stage was simple to identify in the test specimen. Transition was considered to occur at the transition plane, or delamination after which significant bulging was observed. This can be seen in the side view of Figure 3.11. As the separation plane forms and the rear panel begins to bulge, material is significantly drawn in-plane towards the impact point. Thus, the thickness of material undergoing shear plugging, \(t_S\), could be easily measured. The proportion of the panel that was penetrated in the bulging stage, \(t_B\), was simply calculated by subtracting the shear plugging thickness from the original target thickness, \(t\), as shown in Figure 3.15.
Figure 3.15: Shear plugging measurement

Figure 3.16 shows the measured ratio of thickness in shear plugging to total thickness ($t_S/t$) as a function of impact velocity and target thickness for the UHMW-PE composite targets. In this case there is an interdependence between the impact velocity and target thickness because data at higher impact velocities corresponds to data against thicker targets. These results demonstrate that as the impact velocity increases (or the panel thickness increases), the proportion of the material undergoing shear plugging increases. At impact velocities below approximately 500 m/s (or targets less than 10 mm thick), no transition plane was observed as the target failed primarily in bulging. Although there was some evidence of shear failure to the surface plies, as this was only minor and there was no obvious transition plane; thin laminates were considered as having no shear plugging thickness. As the impact velocity increased, the shear plugging thickness increased rapidly such that at impact velocities of 1000 m/s (or targets around 25 mm thick) around 60% of the laminate failed in shear plugging. With further increase in impact velocity to 2000 m/s (or 100 mm thickness) the shear plugging thickness approached around 75% of the laminate.

Figure 3.16: Shear plugging thickness ratio in terms of (a) impact velocity and (b) target thickness

3.3.2.3 Bulge Development

Figure 3.12 shows the formation of a bulge when a UHMW-PE composite panel is impacted close to the ballistic limit. The formation of a bulge is an important characteristic of UHMW-PE composite, characterised by the propagation of the hinge (or bulge radius) and apex as shown in Figure 3.17. The propagation of the hinge is characteristic of the transverse wave properties of the material. For UHMW-PE composite stripes and thin laminates, the hinge propagates at a
constant velocity that can be predicted using classical yarn impact theory (Smith et al., 1958), as described in the literature review chapter and shown in Chocron et al. (2013). For thicker targets, the propagation of the hinge is complicated by the fact that the target is progressively penetrated, the deceleration of the projectile and bending resistance from the thicker laminate. The apex position provides an indication of the approximate position of the projectile. More critically, the velocity of the apex can be determined knowing its position with respect to time. The position of apex is an important considerations in armour applications where the back face deformation is used to determine injury criteria. The back face deformation is typically measured with a soft backing material such as clay. While these are useful, they only provide a measure of the maximum displacement, which can be impeded by the presence of the clay. The velocity of the apex is not captured in these types of test, which is equally if not more important than the maximum back face deformation for injury criteria because the momentum and impulse loading onto the subject behind the armour can be lethal.

In this work, the use of a high speed camera allows tracking of the bulge formation. Position measurement calibration was performed along the expected projectile line of flight, however variability in the line of flight and resolution of the camera resulted in a measurement error of approximately ±3 mm. Furthermore, since the high speed camera can not be configured to capture the exact instant of impact, there can be an error associated with the time of impact of up to 50 μs (the time between frames). The hinge and apex positions were measured for the 10 mm, 20 mm and 36 mm target impacted by 20 mm FSP, just below the ballistic limit velocity. The details of the impact velocity ($V_1$) compared to the ballistic limit velocity ($V_{50}$) for these targets are shown in Table 3.3.

Figure 3.17: Bulge hinge and apex position of 36 mm target impacted by 20 mm FSP at 888 m/s 450 μs after impact
Table 3.3: Bulge measurement tests

<table>
<thead>
<tr>
<th>Target Thickness (mm)</th>
<th>$V_{50}$ (m/s)</th>
<th>$V_1$ (m/s)</th>
</tr>
</thead>
<tbody>
<tr>
<td>10</td>
<td>394</td>
<td>365</td>
</tr>
<tr>
<td>20</td>
<td>620</td>
<td>615</td>
</tr>
<tr>
<td>36</td>
<td>901</td>
<td>888</td>
</tr>
</tbody>
</table>

The results of the apex and hinge positions are shown in Figure 3.18 for up to 500 μs after impact. After this time, the hinge has reached the support for some targets (10 mm target) so no meaningful comparison can be made between the bulge development beyond this time. As seen in Figure 3.18(a), the extent of the apex is largely dependent on the impact velocity, with targets impacted at higher impact velocities showing greater apex extent at a given time. However, the propagation of the hinge as shown in Figure 3.18(b) in general does not show the same dependency, even though the dependency on impact velocity exists at a more fundamental level (i.e. impact on fibre, yarn, strips and very thin laminates). For these targets, the projectile is decelerating and the proportion of the target thickness under bulging deformation are different, leading to more complex hinge propagation rates.

![Figure 3.18](image-url)

Figure 3.18: Development of bulge hinge and apex position for targets impacted by 20 mm FSP below the ballistic limit, impact velocity shown in legend

The 10 mm thick panel (which is the thinnest panel tested) was found to exhibit bulging deformation only (as shown in the previous section). Therefore the apex and hinge propagation responses should be similar to the response of fibre, yarn, strips and thin laminates (a straight position-time curve for both apex and hinge). There is a curvature in the apex and hinge curves (Figure 3.18(b)) because the projectile is decelerating in this case unlike at the fibre level. In contrast the thicker 20 mm and 36 mm panels exhibit two-stage penetration and display a plateau in the hinge position. This is because a transition plane forms for these thicker panels when a bulge develops under impact. As the bulge grows in these thicker targets, material is drawn inwards, but is resisted at the transition interface which does not exist for the thinner 10 mm panel. Close to the impact site the resistance to drawing is low because delamination planes already exist from the propagation of the initial shock wave prior to bulging. Further away from the penetration site, the presence of delaminations planes diminishes as the shock weakens. As such resistance to drawing increases further away from the impact site as the crack that forms the transition plane must propagate further in order for material to be drawn in. This is evident in Figure 3.18(b) where there are two distinct slopes in the hinge position.
The change in the hinge and apex position with respect to the change in time was determined from the results in Figure 3.18, to estimate the velocity of the apex and hinge, the results are plotted in Figure 3.19. Although the velocity was estimated over quite a large time increment (50 μs - limited by the frame rate of the high speed camera), the results provide an approximation of the velocity and the deceleration behaviour of the hinge and apex. For these calculations, the first data point at time = 0 is omitted due to the uncertainty in the initial impact time. As shown in Figure 3.19(a), the apex velocity profile is reasonably smooth and gradual, indicating a relatively constant deceleration. The velocity profile is highest on the 36 mm target, followed by the 20 mm and 10 mm target, reflecting the relative impact velocity of these targets. The velocity profile of the hinge in Figure 3.19(a), shows significant initial deceleration and levels off 250 μs after initial impact. As the hinge propagates, the area and therefore mass of the bulge increases by approximately the square of the bulge radius. Since momentum in the system is conserved (constant), momentum transfer to the bulge is proportional to the mass increase. For this case the propagation velocity must decrease in proportion, leading to significant deceleration of the hinge. Therefore, the formation of the bulge accounts for a significant portion of the energy absorbed by way of momentum transfer from the projectile to the bulge.

![Figure 3.19: Bulge apex and hinge propagation velocity](image)

### 3.4 Efficiency of UHMW-PE Composite Against Fragment Impact

While it is widely known that UHMW-PE composite is an effective material against ballistic threats, no studies have been reported that quantitatively assesses the ballistic efficiency of the material compared to other armour materials. The experimental ballistic limit results obtained in this study for a wide range of target thickness against the two different calibre projectiles is used in this section to compared the efficiency of UHMW-PE composite against FSPs to some common metallic and fibre-reinforced composites.

Efficiency, in this instance, is assessed in terms of the mass per unit area of an armour system required to protect against a certain threat, relative to the mass of a traditional armour material such as rolled homogeneous armour steel (RHA) required to provide the same level of protection. Mass efficiency is defined as:

\[
E_m = \frac{t_{ref} \times \rho_{ref}}{t \times \rho}
\]  

(3.1)
where \( t \) is the armour thickness required to defeat the threat, \( \rho \) is the density of the armour material, and the subscript \( \text{ref} \) refers to the reference material (e.g. RHA). Another measure of armour efficiency relates to the armour space claim, or the thickness of an armour system required to protect against a certain threat, relative to the thickness of a traditional armour material required to provide the same level of protection. Space efficiency is defined as:

\[
E_s = \frac{t_{\text{ref}}}{t}
\]  

(3.2)

A space and/or mass efficiency value greater than 1.0 indicates a thinner and/or lighter armour than the reference configuration against the same threat. These efficiency factors have been used to evaluate the effectiveness of a range of armour materials including aluminium (Hohler et al., 1995) and ceramics (Madhu et al. (2006) and Savio et al. (2011)), but have not been investigated for UHMW-PE composite nor any other fibre-reinforced composite. Not only are the efficiency factors important for armour design considerations, but a deeper understanding of the material properties and the penetration mechanisms that underpin the relative ballistic efficiency is critical for further improvements to the protection levels provided by armour materials.

### 3.4.1 Ballistic Testing

Further ballistic tests were performed on two armour steels to provide a baseline for performance comparison. The two steels were BISPLATE® High Hardness Armour (HHA) plate, a nominally 500 HB (Brinell hardness) steel conforming to MIL-DTL-46100 (Department of Defense, 2008), and BISPLATE® High Impact Armour plate (Class 1), a nominally 350 HB steel conforming to a Class 1 armour plate as per MIL-A-12560 (Department of Defense, 2013). High Impact Armour is considered analogous to rolled homogenous armour (RHA), and will be referred to herein as RHA steel.

Similar to the ballistic test on UHMW-PE composite, the steel targets were impacted by MIL-DTL-46593B (Department of Defense, 2006) spec 12.7 mm and 20 mm calibre FSPs. Complete penetration for the steel targets was defined by light transmission through 0.1 mm thick Al 5005–H34 witness plates spaced 100 mm from the rear face of the target plate. The results of this test are summarised in Table 3.4.

<table>
<thead>
<tr>
<th>Material</th>
<th>Thickness (mm)</th>
<th>Projectile</th>
<th>( V_50 ) (m/s)</th>
<th>( \sigma ) (m/s)</th>
<th>( c_v ) (%)</th>
</tr>
</thead>
<tbody>
<tr>
<td>RHA steel</td>
<td>6.6</td>
<td>12.7 mm FSP</td>
<td>612.1</td>
<td>8.5</td>
<td>1.4</td>
</tr>
<tr>
<td>RHA steel</td>
<td>15.1</td>
<td>12.7 mm FSP</td>
<td>1319.9</td>
<td>14.3</td>
<td>1.1</td>
</tr>
<tr>
<td>RHA steel</td>
<td>10.3</td>
<td>20 mm FSP</td>
<td>591.9</td>
<td>18.5</td>
<td>3.1</td>
</tr>
<tr>
<td>RHA steel</td>
<td>15.0</td>
<td>20 mm FSP</td>
<td>698.4</td>
<td>17.4</td>
<td>2.5</td>
</tr>
<tr>
<td>HHA steel</td>
<td>6.2</td>
<td>12.7 mm FSP</td>
<td>612.5</td>
<td>7.8</td>
<td>1.3</td>
</tr>
<tr>
<td>HHA steel</td>
<td>10.1</td>
<td>20 mm FSP</td>
<td>593.8</td>
<td>10.9</td>
<td>1.8</td>
</tr>
<tr>
<td>HHA steel</td>
<td>12.5</td>
<td>20 mm FSP</td>
<td>638.0</td>
<td>16.8</td>
<td>2.6</td>
</tr>
</tbody>
</table>

### 3.4.2 Ballistic Efficiency Compared to Metal

Figure 3.20 plots the ballistic limit test results of Dyneema® HB26, RHA steel and HHA steel in terms of a non-dimensionalised areal density \( AD_t A_p/m_p \), with additional data for HHA and
RHA steels taken from Gooch et al. (2007) and Jones et al. (2007). Data on aluminium alloy 5059-H131 from Showalter et al. (2007) is also included in the plot. Similar to UHMW-PE composite, the 12.7 mm and 20 mm FSP data for RHA steel, HHA steel and Al 5059-H131 collapse on the same curve for each material when plotted with respect to $AD/m_p$.

![Figure 3.20: Ballistic limit of UHMW-PE composite for 12.7 mm and 20 mm calibre fragment simulating projectiles, compared to RHA, HHA, and Al 5059-H131 in terms of non-dimensional areal density. (C) in legend refers to current study](image_url)

The mass efficiency of UHMW-PE is plotted in Figure 3.21 for the 12.7 mm and 20 mm FSPs against that of HHA steel and Al 5059-H131, with RHA steel as the reference material. Although only limited results are presented for RHA steel, they are consistent with data from additional unpublished sources. In Figure 3.21 the results for HB26 Analytical is obtained by using the ballistic limit analytical model for UHMW-PE composite derived in Chapter 4 to estimate efficiency values for higher target areal densities than what was obtained experimentally. The results for RHA is designated an efficiency value of one since it is used as the reference material. For both 12.7 mm and 20 mm FSPs, the mass efficiency of Dyneema® HB26 is much higher than HHA steel and aluminium alloy for the range of target areal densities investigated. The mass efficiency is shown to approach approximately 4.5 for very thin panels with both projectile calibres. As the areal density is increased, the mass efficiency is shown to initially decrease until panel areal densities of approximately 50 kg/m², above which the mass efficiency is seen to asymptote at 3.0 for 12.7 mm FSPs and 3.8 for 20 mm FSPs.
Figure 3.21: Mass efficiency relative to RHA steel of targets against (a) 12.7 mm FSPs and (b) 20 mm FSPs in terms of target areal density

The resistance to penetration of thin UHMW-PE composite is dominated by membrane stresses when impacted at velocities close to the ballistic limit. As the target thickness decreases, the membrane response becomes increasingly efficient due to decreasing transverse constraint provided by adjacent plies (Cunniff, 1992). For thicker UHMW-PE panels, penetration occurs in two stages as shown in section 3.3.

Figure 3.22(a) plots the shear plugging ratio with respect to the non-dimensional areal density. This shows that transition from single- to two-stage penetration occurs at a non-dimensional areal density of about 0.08 for tests close to the ballistic limit against FSPs. Above this value, the thickness of UHMW-PE composite penetrated in shear plugging is related to the total thickness of the panel by a power law fit. As the ratio of thicknesses between the two penetration phases stabilises, i.e. greater than approximately 0.4 on the non-dimensional areal density axis, the mass efficiency of the UHMW-PE composite is also found to stabilise in Figure 3.22(b). This suggests that the membrane tension mechanism is more efficient at protecting against fragment threats on a mass basis than the shear plugging mechanism. This is consistent with other research which showed UHMW-PE composite is more effective when used on the back of an aluminium plate than on the front (O’Masta et al., 2014). This is because UHMW-PE composite is loaded in membrane tension when placed on the back of a target and is a major reason the material is widely used as a spall liner.
RHA steel was found to fail by adiabatic shear for the range of thicknesses investigated in this work. Adiabatic shear failure is common amongst metals under ballistic impact by blunt projectiles. This occurs when the “rate of work hardening is less than the rate of thermal softening due to the conversion of mechanical work to heat in plastic flow” (Woodward, 1978, pp. 599). When this occurs the material strength is significantly reduced as a result of the thermal load, leading to failure around a narrow shear band and the formation of a plug around the perimeter of the projectile (Woodward (1978), Dikshit et al. (1995) and Børvik et al. (2001)). The results in Figure 3.21 also show that the efficiency of HHA steel is lower than RHA steel against FSPs. This is consistent with other work which has shown that metals of higher hardness typically have lower ballistic performance against FSPs due to a greater susceptibility to adiabatic shear plugging (Gooch et al., 2007).

The space efficiency of UHMW-PE composite, HHA steel and Al 5059-H131 relative to RHA steel is plotted in Figure 3.23 for the 12.7 mm and 20 mm FSPs. For both calibre FSPs, the space efficiency of Dyneema® HB26 is between 40% to 60% that of RHA steel. The space efficiency of UHMW-PE composite is higher than Al 5059-H131 below areal densities of 65 kg/m² for 12.7 mm FSPs and 110 kg/m² for 20 mm FSPs.
Figure 3.23: Space efficiency relative to RHA steel of targets against (a) 12.7 mm FSPs and (b) 20 mm FSPs in terms of target areal density.

The ballistic performance of metals (including armour steels) and ceramics is sensitive to projectile scale, i.e. larger projectiles are more efficient penetrators (Magness and Farrand (1990), Anderson et al. (1993) and Anderson et al. (1996)). This is because the level of damage accumulates with respect to absolute time and not scaled time, i.e. sub-scale impacts will accumulate less damage (on the target) than full scale impacts for the same impact velocity and time period. Furthermore, the strain rates (and therefore strain rate hardening) are higher for sub-scale projectiles compared to full-scale projectiles (Anderson et al. (1993) and Anderson et al. (1996)). To illustrate this the ballistic limit results of RHA steel and UHMW-PE composite are plotted in terms of both target areal density and non-dimensionalised areal density for the 12.7 mm and 20 mm calibre projectiles in Figure 3.24. The data can be approximated by a linear fit in all instances. When plotted in terms of target areal density, Figure 3.24(a) and Figure 3.24(b), there is little to distinguish between the RHA steel and UHMW-PE composite. For both materials the slope of the 20 mm FSP curve is less than that of the 12.7 mm curve for a given target areal density, as would be expected considering the relative projectile diameter and masses. In terms of non-dimensionalised areal density ratio, Figure 3.24(c) and Figure 3.24(d), however, there is a marked difference between the curves for each material. UHMW-PE composite shows effectively no difference between the two projectile calibres, while for RHA steel the 20 mm FSP is a more effective penetrator, i.e. a lesser ballistic limit for a given target areal density.
This shows that RHA steel is clearly sensitive to the projectile scale but UHMW-PE composite is not. Other composites such as GFRP, CFRP and AFRP also do not show sensitivity to projectile scale as the ballistic performance scales with the non-dimensional areal density (Cunniff, 1999). Unlike metals under adiabatic shear plugging, composites are penetrated progressively so damage is not accumulated until the target is fully perforated. This is because of the layered construction of the composite, which allows it to fail ply by ply as the projectile progresses through the target. Since the laminate plies are orders of magnitude smaller than the diameter of the projectile, projectile scaling effects are negligible in composites. For metals under adiabatic shear plugging however, there is either no perforation with small region of plastic deformation beneath the projectile or a shear plug forms and the target is perforated. This penetration mode depends on the level of accumulated damage and therefore metals exhibiting this failure mode have a higher sensitivity to projectile scale.

### 3.4.3 Ballistic Efficiency Compared to Fibre-Reinforced Composites

Figure 3.25 compares the $V_{50}$ values of Dyneema® HB26 with other fibre-reinforced composites in terms of the non-dimensional areal density ratio. Experimental data for Kevlar® KM2/
polyvinylbutyral (AFRP), E-glass/polyester (GFRP) and carbon fibre/epoxy (CFRP) were taken from Cunniff (1999). In Cunniff (1999), AFRP had a fibre content of 82% to 85% by weight, while GFRP and CFRP had a fibre content of approximately 70%. Unfortunately no information is provided in terms of the composite construction (i.e woven or prepreg and stacking sequence). In comparison, the UHMW-PE composite used in this work has a fibre content of approximately 80% by weight and is composed of unidirectional prepreg with a [0/90]_n stacking sequence as described in section 3.1. The experimental data reported in Cunniff (1999) were of targets impacted by small calibre (0.12 g to 8.2 g) steel and tungsten projectiles with a length-to-diameter ratio of approximately 1.0. No further information is provided on the geometry of the projectile. Assuming a right circular cylinder however, these projectiles can have diameters up to 11 mm, similar to the 12.7 mm FSPs used in this work.

Figure 3.25: Ballistic limit velocity against non-dimensionalised areal density ratio for various fibre-reinforced composites

UHMW-PE composite is shown to provide higher ballistic performance over the other fibre-reinforced composites for the entire range of non-dimensional areal density values. The ballistic performance of fibre-reinforced composites is highly dependent on the mechanical properties of the fibre, namely tensile modulus, strain-to-failure, tensile strength or tenacity, and the longitudinal wave speed (Cunniff, 1999). Table 3.5 provides a comparison of the relevant fibre properties.

<table>
<thead>
<tr>
<th>Fibre</th>
<th>Density (kg/m³)</th>
<th>Modulus (GPa)</th>
<th>Strength (GPa)</th>
<th>Failure strain (%)</th>
<th>Tenacity (N/tex)</th>
<th>Wave Speed (km/s)</th>
<th>Source</th>
</tr>
</thead>
<tbody>
<tr>
<td>UHMW-PE (Dyneema® SK76)</td>
<td>970</td>
<td>116</td>
<td>3.6</td>
<td>3.8</td>
<td>4.7</td>
<td>10.9</td>
<td>Van Dingenen (2001)</td>
</tr>
<tr>
<td>Aramid (Kevlar® KM2)</td>
<td>1440</td>
<td>83</td>
<td>3.4</td>
<td>3.6</td>
<td>2.4</td>
<td>7.6</td>
<td>Cunniff (1999)</td>
</tr>
<tr>
<td>Carbon</td>
<td>1800</td>
<td>227</td>
<td>3.8</td>
<td>1.8</td>
<td>2.1</td>
<td>11.2</td>
<td>Cunniff (1999)</td>
</tr>
<tr>
<td>E-Glass</td>
<td>2550</td>
<td>74</td>
<td>3.5</td>
<td>4.7</td>
<td>1.4</td>
<td>5.4</td>
<td>Cunniff (1999)</td>
</tr>
</tbody>
</table>

As mentioned in the literature review chapter, a high longitudinal wave speed allows the material to more rapidly distribute the impact load, while a high tensile strength increases the amount
of stress the fibre can carry prior to failure. Both the longitudinal wave speed and tensile strength of UHMW-PE fibres are higher than that of aramid fibres, contributing to improved ballistic performance. Furthermore, the tenacity of aramid fibres is approximately 35% lower than UHMW-PE fibre, which is similar to the relative ballistic performance of the two materials for a given areal density. Carbon and glass fibres have high longitudinal wave speeds and tensile strengths; in the case of carbon fibre even exceeding those of UHMW-PE fibre, however their ballistic performance is poor in comparison. This is not only due to the lower fibre tenacity (carbon fibre has higher tenacity but lower ballistic performance compared to glass), but is related to the microstructure of the different fibre types that promotes different penetration and failure mechanisms.

UHMW-PE and aramid fibres are micro-fibrillar and therefore under through-thickness loading induced by an impact, their performance in the fibre direction is not significantly affected (Cunniff et al. (2002) and Cheng et al. (2004)). Carbon fibre has an anisotropic microstructure, which behaves in a brittle manner under through-thickness loading, leading to failure by fibre crushing rather than redistributing the load in membrane tension (Woodward et al., 1994). Glass fibres, although isotropic in microstructure, are also sensitive to compression failure under through-thickness loading (Woodward et al., 1994). Similarly, Hazell and Appleby-Thomass (2012) reviewed the ballistic performance of carbon and glass fibre-reinforced composites and showed that these materials experience localised fibre failure under through-thickness ballistic loading, so that any advantage of high tensile properties for membrane loading is nullified.

The absence of areal density and projectile data for the aramid, glass and carbon fibre composites in Cunniff (1999) does not permit an evaluation of mass and space efficiency in terms of target areal density, as performed in the previous section for metals. Instead, a linear relationship is derived for each composite material from Figure 3.25, from which the mass and space efficiency are determined as follows:

\[
E_m = \frac{(AD_t A_p/m_p)_{ref}}{(AD_t A_p/m_p)} = \frac{(AD_t)_{ref}}{AD_t} \quad \text{and} \quad E_S = E_m \left(\frac{\rho}{\rho_{ref}}\right)
\]  

(3.3)

Figure 3.26 and Figure 3.27 show the mass efficiency and space efficiency of the various composites, where UHMW-PE composite is used as the reference material. Data is only taken for velocities greater than 250 m/s because below this value the ballistic limit performance is not linear.
Figure 3.26: Mass efficiency of fibre-reinforced composites in terms of target ballistic limit

Figure 3.27: Space efficiency of fibre reinforced composites in terms of target ballistic limit

The mass efficiency of UHMW-PE composite is found to be at least 30% higher than AFRP, 50% higher than GFRP and 60% higher than CFRP. The space efficiency of GFRP and AFRP are higher than that of UHMW-PE for $V_{50}$ values greater than about 500 m/s and 1000 m/s respectively, with GFRP having the highest space efficiency of all four composites above a $V_{50}$ of around 500 m/s. At lower impact velocities (less than 500 m/s) the higher non-linear mass and space efficiency of UHMW-PE composite compared to the three other composite materials is due to the higher efficiency of thinner UHMW-PE composite under membrane
loading (thinner composites have lower constraint to bulge deformation (Cunniff, 1992)). At higher velocities (greater than 750 m/s), the efficiency stabilises because UHMW-PE composite exhibits two stage penetration where the efficiency of the shear plugging stage does not change with thickness (see Figure 3.22).

3.5 Conclusion

In this chapter, the response of thick UHMW-PE composite under ballistic impact was experimentally investigated. The aim was to determine the penetration and failure mechanisms of thick UHMW-PE composite, understand the transition of the material from one penetration mode to another, and to assess the ballistic performance of UHMW-PE composite compared to other armour materials.

Depth of penetration tests were performed on semi-infinite targets to understand the penetration and failure mechanisms of thick targets without target breakout. Analysis of the fibres in the penetration cavity using scanning electron microscopy showed penetration by shear plugging, with fibre shearing the dominant failure mechanism. Furthermore, extensive delamination failure was observed, with delaminations extending through the entire target beyond the maximum penetration depth. These delaminations are possibly formed from the propagation of the pressure wave and the interaction of releases waves on the back of the target. These delaminations are important in the breakout of targets of a finite thickness.

Ballistic limit tests were performed for panel thicknesses up to 100 mm using 12.7 mm and 20 mm calibre FSPs. Analysis of the targets show penetration of thick UHMW-PE composite occurs in two stages. An initial shear plugging stage, where penetration occurs in fibre shearing with no deflection of the target, similar to the DoP tests. The shear plugging stage was followed by bulging of the target back face, where a sub-laminate breaks away from the target and undergoes large deflection and penetration in fibre tension, commonly observed in fabrics and thin composites. Characterisation of the fibre fracture morphology throughout the penetration cavity using scanning electron microscopy showed fibre failure in transverse shear in the shear plugging stage, and tensile fibre failure in the bulging stage. Delamination occurred in both stages of penetration, and was important in forming a transition plane between the sections of the target undergoing either shear plugging or bulging. The position of the transition plane was characterised for the wide range of test configurations in this work. Transition between the two stages of penetration was found to be dependent on the impact velocity and target thickness, with an increasing proportion of the target penetrated by shear plugging with increased impact velocity or target thickness. The mechanisms for transition are complex and are difficult to observe experimentally, so further investigations on transition will be investigated numerically in Chapter 5.

The effectiveness of UHMW-PE composite against FSPs was also evaluated and compared to other common armour materials in this chapter in terms of the mass and space efficiency. UHMW-PE composite was found to be much more mass efficient than the two armour steels and aluminium. The mass efficiency of UHMW-PE composite against FSPs was found to be between 300% to 450% higher than the metals studied in this work. The space efficiency of UHMW-PE composite was however about 50% lower than both armour steels. Compared to aramid, glass, and carbon fibre-reinforced composites, UHMW-PE composite has a higher mass efficiency (typically between 130% to 160%), although it was less space efficient than the glass and aramid fibre-reinforced composites for target ballistic limits above 1000 m/s. The efficiency of UHMW-PE composite was shown to be highest for thin targets where bulging and membrane
tension loading was dominant. This increased with thinner targets because thinner targets provides less transverse constraint to bulge deformation. For thicker targets under two-stage penetration where shear plugging dominates, the efficiency levels off because the penetration resistance under shear plugging does not change with thickness.
Chapter 4

Analytical Model for the Ballistic Limit of Thick UHMW-PE Composite

An analytical model describing the penetration of thick UHMW-PE composite has not been developed, and the literature review showed existing single stage models are inadequate for predicting the ballistic performance of thick targets (Figure 2.7). In Chapter 3, the penetration and failure response of thick UHMW-PE composite under ballistic impact by FSPs was characterised. In this chapter, this knowledge is used to derive an analytical model that describes the key governing penetration and failure modes. The analytical model allows prediction of the ballistic limit velocity for thick UHMW-PE composite impacted by a blunt projectile. The predictions are compared to experimental ballistic limit results to demonstrate the capacity of the analytical model.

The work in this chapter has been published in the following peer-reviewed journal publication:


4.1 Perforation Model

The experimental investigation presented in Chapter 3 showed thick UHMW-PE composite panels are penetrated in two stages: initial penetration by shear plugging where fibre shearing is dominant, followed by a bulging stage where a part of the panel behaves like a membrane failing in tension. These two stages are treated separately in the model, with the shear plugging stage described using energy conservation and the bulging stage described using momentum conservation and classical yarn impact theory. The transition between shear plugging and bulging behaviour of thick UHMW-PE composite, seen in experimental testing, is incorporated with the inclusion of the shear plugging ratio as characterised in Chapter 3. An energy balance is used between the projectile kinetic energy and the energy absorbed by the target, in order to derive an expression for the ballistic limit velocity. The assumptions used in these equations are based on the experimental observations and discussions presented in the previous chapter.
4.1.1 Shear Plugging

The following assumptions are made (and the effect of the assumption on energy absorbed) for the shear plugging stage:

- Non-deforming projectile (under-prediction of energy absorbed). The significantly higher hardness of the steel FSP compared to UHMW-PE composite means the projectile is mostly undeformed during the initial penetration stage;
- Penetration by through-thickness shear. Fibre shearing is the dominant failure mechanism in this stage of penetration;
- No forward momentum transfer to the plug, as plug material is considered to be ejected from the target front face (under-prediction of energy absorbed);
- Energy absorbed due to fibre tension and compression is negligible (under-prediction of energy absorbed);
- Energy associated with delamination, ply splitting and fibre-matrix debonding is ignored (under-prediction of energy absorbed), as the matrix properties of UHMW-PE composite are very low;
- Shock-induced heating and melting is ignored (over-prediction of energy absorbed), as the melting temperature and thermal conductivity of UHMW-PE composite is low (Prevorsek et al., 1994).

The energy to perforate material around the perimeter of a blunt projectile is equal to the work required to produce a shear plug, where the shear area is the circumference of the projectile multiplied by the thickness of the material in the shear plugging stage. This is given by:

$$E_S = \int_0^{t_S} \tau_{\text{max}} (2\pi r_p) t dt = \tau_{\text{max}} \pi r_p \frac{t^2}{2}$$

where $E_S$ is the energy absorbed in shear plugging, $\tau_{\text{max}}$ is the through-thickness shear strength of the laminate, $r_p$ is the projectile radius, and $t$ and $t_S$ is the target thickness and the thickness under shear plugging respectively. Similar equations that describe the development of a shear plug have been derived elsewhere (Woodward (1978), Woodward and Cimpoeru (1998), Zhang et al. (1998) and Atkins (2012)).

Quasi-static through-thickness shear tests on UHMW-PE composite was performed by Lässig et al. (2015) using a modified fixture that combines the capabilities of four-point bend and rail shear tests. In these tests the slow loading rate caused fibre realignment and ultimate failure was not reached due to limits in the fixture displacement. To date, no tests have been reported that characterise the through-thickness shear strength of UHMW-PE composite under quasi-static or higher strain rates.

The through-thickness shear strength of Kevlar®-epoxy composite was measured by Riedel et al. (2003a) to be approximately 150 MPa using the same test rig in Lässig et al. (2015). In comparison, the tensile strength was measured to be in the range of 250 MPa to 350 MPa. The through-thickness shear strength is approximately half the tensile strength, which is the same as the maximum shear stress given by the principal stress or Tresca yield theory. Given the similarity between Kevlar® and UHMW-PE composites (i.e used in ballistic applications,
high fibre volume fraction and micro-fibrillar fibre micro-structure), the through-thickness shear strength, \( \tau_{\text{max}} \), of UHMW-PE composite is approximated using the maximum shear stress from principal stress or Tresca yield theory. According to these theories the maximum through-thickness shear stress is half of the uniaxial tensile strength:

\[
\tau_{\text{max}} = \frac{\sigma_{\text{max}}}{2}
\]  
(4.2)

In order to utilise high strain-rate tensile data on UHMW-PE yarns, the stress is described in terms of the fibre elastic modulus \( E_f \), strain-to-failure \( \varepsilon_{\text{max}} \), volume fraction \( v_f \), and adjusting for the cross-ply layup according to the rule-of-mixtures (assuming only longitudinal fibres carry the load in a tension test):

\[
\tau_{\text{max}} = \frac{\sigma_{\text{max}}}{2} = \frac{E_f \varepsilon_{\text{max}} v_f}{4}
\]  
(4.3)

This equation assumes the fibres exhibit linear stress-strain behaviour, which is consistent with experimental tension test of UHMW-PE under relatively high strain rates as discussed in section 2.4.1.

Substituting 4.3 into 4.1 gives the energy absorbed in the shear plugging phase in terms of the fibre tension properties:

\[
E_S = \frac{1}{4} E_f \varepsilon_{\text{max}} v_f \pi r_p t_S^2
\]  
(4.4)

### 4.1.2 Bulging

The assumptions made (and the effect of assumption on energy absorbed) for bulging stage are:

- A deformed projectile. Since the bulging stage follows the shear plugging stage, the projectile would have deformed quite significantly by the time the target begins to bulge, such that the stress concentration on the composite around the projectile perimeter is significantly reduced. This assumption allows stress concentration to be ignored during the bulging stage. Figure 4.1 shows the deformation of some recovered 20 mm FSPs which show increasing deformation for impact velocities above 500 m/s, for which this analytical model is applicable for;
- Failure by fibre tension. Fibre tensile failure was found to be dominant under this stage of penetration;
- The panel under this stage is treated as a membrane. The large degree of delamination in this stage allows the individual plies to respond independently. The individual plies have negligible bending resistance (under-prediction of energy absorbed);
- As with the shear plugging stage, energy associated with delamination and shock-induced heating and melting is ignored.
According to conservation of momentum, the momentum of the system just before and after the initial breakout of the bulge, as shown in Figure 4.2, is equal:

\[ m_p V_B = (m_p + m_B) V \]  

(4.5)

\[ V_B = V \left( 1 + \frac{m_B}{m_p} \right) = V \left( 1 + \beta^2 \frac{\pi r_p^2 t B \rho_t}{m_p} \right) = V \left( 1 + \beta^2 \frac{t B \rho_A p}{m_p} \right) \]  

(4.6)

where \( V_B \) is the projectile velocity just before breakout, \( m_B \) is the mass of the target in the bulging stage initially involved in the momentum transfer, \( V \) is the velocity of the combined projectile and bulging mass just after initial breakout, \( A_p \) is the projectile presented area, \( \rho_t \) is the target density, and \( \beta \) is a non-dimensional multiplier for \( r_p \) as shown in Figure 4.2. The \( \beta \) parameter accounts for the fact that the initial momentum transfer to the target just after breakout occurs over a radius larger than the projectile radius, otherwise shear plugging persists. This concept was introduced by Walker (1999) and adopted by Phoenix and Porwal (2003) in their membrane analytical models.

Smith et al. (1958) classical theory describes the wave propagation of a textile yarn impacted transversely. This theory is discussed in more detail in the literature review chapter. The relationship between the impact velocity \( V \) (which is constant after impact because the fila-
ment mass is negligible), the speed of wave propagation $c$, and the strain that develops as the longitudinal wave propagates through the filament is given by (Chocron et al., 2013):

$$V = c\sqrt{2\varepsilon\sqrt{\varepsilon + \varepsilon^2} - \varepsilon^2} \quad (4.7)$$

This relationship has been shown to be applicable to UHMW-PE composite (Chocron et al., 2013). The speed of wave propagation according to Smith et al. (1958) is:

$$c = \sqrt{\frac{1}{M} \frac{dT}{d\varepsilon}} \quad (4.8)$$

where $M$ is the mass per unit length of the unstrained material, $dT/d\varepsilon$ is the slope of the tension force-strain curve. UHMW-PE composite exhibits linear stress-strain behaviour under high strain rate in tension (Russell et al., 2013), therefore the derivative term reduces to the tension-strain ratio. For a cross-ply composite it is assumed that only the longitudinal fibres carry the tensile load. The tensile force is therefore adjusted to account for the cross-ply layup and fibre volume fraction:

$$\frac{dT}{d\varepsilon} = \frac{T}{\varepsilon} = \frac{\sigma_f v_f A}{2\varepsilon} \quad (4.9)$$

where $T$ is the tension force, $\sigma_f$ is the stress in the fibres, $v_f$ is the fibre volume fraction, and $A$ is the area over which the load is applied.

Substituting 4.8 and 4.9 into 4.7, $\sigma_f/\varepsilon$ for $E_f$, the fibre elastic modulus, and $M$ for $\rho_f A$ (where $\rho_f$ is the fibre density) gives:

$$V = \sqrt{\frac{E_f}{\rho_f}} \sqrt{\frac{v_f}{2}} \sqrt{2\varepsilon\sqrt{\varepsilon + \varepsilon^2} - \varepsilon^2} \quad (4.10)$$

Substituting 4.10 into 4.6 yields:

$$V_B = \left(1 + \beta^2 \frac{t_B \rho_t A_p}{m_p} \right) \sqrt{\frac{E_f}{\rho_f}} \sqrt{\frac{v_f}{2}} \sqrt{2\varepsilon\sqrt{\varepsilon + \varepsilon^2} - \varepsilon^2} \quad (4.11)$$

If small strain is assumed and the squared strain terms are in turn removed, equation 4.11 reduces to the equations derived by Phoenix and Porwal (2003) (without adjustment for the fibre volume fraction) to describe a fabric membrane impacted by a projectile. The Phoenix and Porwal (2003) model further incorporates strain concentration around the projectile, which decays to unity for thicker targets or large projectile to bulge radius ratios. As the projectile deformation increases with increasing impact velocity for thicker targets (as shown in Figure 4.1) the strain concentration diminishes (Phoenix and Porwal, 2003). Since this model applies to thick composites that are penetrated in two stages, strain concentration is ignored based on the assumption the projectile is deformed by the time the target begins to bulge. Further, the small strain assumption is not applied in this formulation as compared to Phoenix and Porwal (2003). Substituting the strain $\varepsilon$ for the fibre failure strain $\varepsilon_{max}$, the energy absorbed in the bulging stage is given by:

$$E_B = \frac{1}{2} m_p V_B^2 = \frac{1}{2} m_p \left(1 + \beta^2 \frac{t_B \rho_t A_p}{m_p} \right)^2 \frac{E_f v_f}{\rho_t} \frac{1}{2} \left(2\varepsilon_{max} \sqrt{\varepsilon_{max} + \varepsilon_{max}^2} - \varepsilon_{max}^2 \right) \quad (4.12)$$
4.1.3 Ballistic Limit

At the ballistic limit, the initial kinetic energy of the projectile is assumed equal to the energy absorbed during the two stages of penetration, so that:

\[ E_{total} = \frac{1}{2} m_p V_{BL}^2 = E_S + E_B \]  \hspace{1cm} (4.13)

Substitute 4.4 and 4.12 into 4.13 and rearranging for \( V_{BL} \):

\[ V_{BL} = \sqrt{\frac{E_f \varepsilon_{max} v_f \pi r_p t_S^2}{2 m_p}} + \left( 1 + \beta^2 \frac{t_B \rho_t A_p}{m_p} \right) \frac{E_f v_f}{\rho_f} \left( 2 \varepsilon_{max} \sqrt{\varepsilon_{max} + \varepsilon_{max}^2} \varepsilon_{max}^2 - \varepsilon_{max}^2 \right) } \]  \hspace{1cm} (4.14)

The thickness in the shear plugging and bulging stages can be related to the total thickness using the term \( k \) which defines the shear plugging thickness ratio, so that:

\[ t_S = kt \quad \text{and} \quad t_B = (1 - k)t \]  \hspace{1cm} (4.15)

Substituting 4.15 into 4.14 and substituting \( t \) for \( AD_t/\rho_t \) gives the ballistic limit equation:

\[ V_{BL} = \sqrt{\frac{E_f \varepsilon_{max} v_f \pi r_p k^2 AD_t^2}{2 m_p \rho_t^2}} + \left( 1 + \beta^2 (1 - k) \frac{AD_t A_p}{m_p} \right) \frac{E_f v_f}{\rho_f} \frac{1}{2} \left( 2 \varepsilon_{max} \sqrt{\varepsilon_{max} + \varepsilon_{max}^2 - \varepsilon_{max}^2} \varepsilon_{max}^2 - \varepsilon_{max}^2 \right) \]  \hspace{1cm} (4.16)

For the parameter \( k \), this is determined empirically. Figure 9 plots \( t_S/t \) (or \( k \)) against the non-dimensional areal density parameter \( (AD_t A_p/m_p) \) for test cases impacted within 5% of the calculated \( V_{50} \). The relationship is similar to the one shown in Figure 3.16 in the previous chapter, but the use of data close to the ballistic limit is more suitable for determining an empirical relationship for \( k \). Further, the non-dimensional areal density parameter is used to define the transition between single stage perforation and two-stage perforation, that is, between thin and thick targets.
From Figure 4.3, an empirical power law relationship is determined, such that:

\[ k = C_1 \left( \frac{A_D A_p}{m_p} \right)^{C_2} + C_3 \frac{A_D A_p}{m_p} \geq C_T \]  

(4.17)

where the constants \( C_1, C_2, C_3 \) are curve fit parameters, and \( C_T \) is the transition non-dimensional areal density parameter that defines the transition between thin and thick targets. Equations 4.16 and 4.17 define the ballistic limit equation for a blunt projectile impacting a thick UHMW-PE composite target that undergoes two stages of penetration.

With the exception of the radius multiplier \( \beta \) and the shear plugging ratio \( k \), the new analytical model only contains parameters of the system (i.e projectile mass and diameter and target areal density) or material properties (i.e fibre elastic modulus and failure strain). The radius multiple has been used by Phoenix and Porwal (2003) and Walker (1999) and is a constant for fibre-reinforced composites or fabrics between 1.3 to 1.5 to account for the increase in area involved in momentum transfer in the bulging stage. The shear plugging ratio is the only parameter that needs to be determined from ballistic experiment (and can be easily measured), however the numerical model presented in Chapter 5 is also shown to be capable of accurately predicting this value.

### 4.2 Validation and Discussion

The ballistic limit equation was applied to Dyneema® HB26 and the results compared to experimental \( V_{50} \) data, to demonstrate the applicability of the analytical model for thick UHMW-PE composite targets. The following parameters were used:
• $E_f = 131$ GPa and $\varepsilon_{max} = 0.02$, from high strain-rate tensile test of Dyneema® SK76 fibre (Russell et al., 2013). UHMW-PE fibres have been shown to be highly strain-rate sensitive under creep and quasi-static loading but becomes insensitive at strain rates above $10^{-1}$ s$^{-1}$. The values used here are for strain-rates above $10^{-1}$ s$^{-1}$.

• Target and fibre density is $\rho_t = 980$ kg/m$^3$ and $\rho_f = 970$ kg/m$^3$. Fibre volume fraction $v_f = 0.83$ (Russell et al., 2013).

• Radius multiplier $\beta = 1.4$, which is within the range of 1.3 to 1.5 used in Phoenix and Porwal (2003) and Walker (1999) for a range of fabric-based armour materials. Note that the ballistic limit equation does not show any significant sensitivity to the value of $\beta$ within this range.

• $k$ parameters $C_1 = -0.0013$, $C_2 = -2.5$, $C_3 = 0.74$ and $C_T = 0.08$, from the curve fit shown in Figure 4.3.

In addition, analytical membrane models from Phoenix and Porwal (2003) and Walker (1999) are used to provide predictions for thin targets, and to compare with the developed ballistic limit equation for thick targets. For these models, the values of $E_f$, $\varepsilon_{max}$ and $\beta$ defined above are used.

Figure 4.4 compares the model predictions with the $V_{50}$ data for UHMW-PE composite against three different calibre FSPs, where additional experimental data has been taken from the literature. From these results, the proposed perforation model provides very good agreement with experimental ballistic limits for thick targets. For smaller thicknesses close to the transition point (around $AD_tA_p/m_p = 0.08$), the predictions of the perforation model deviate slightly from the experimental results. This is because the target response in this regime is dominated by the membrane action in bulging, and strain concentration effects near the projectile edge are important to consider. These aspects are accounted for in the membrane models shown, so still provide good predictions of the ballistic limit past the point where the target exhibits two-stage penetration. For thicker targets however, these models provide poor predictions of ballistic performance. These results show the existing membrane models (Walker (1999) and Phoenix and Porwal (2003)) are more suitable for predicting the ballistic limit of thin targets undergoing a single penetrations stage while the new two-stage model is more suitable for thick laminates.
4.3 Conclusion

A new two-stage perforation model for UHMW-PE composite was developed in this chapter that describes the shear plugging and bulging stages of thick UHMW-PE composite. The model allows for prediction of ballistic limit velocity for targets impacted by blunt projectiles. The model describes the shear plugging stage using energy conservation and the bulging stage using momentum conservation and classical yarn impact theory. An energy balance between the projectile kinetic energy and the energy absorbed by the target was considered to derive the ballistic limit velocity.

This model was validated using experimental data for Dyneema® HB26 composite, and showed very good agreement with ballistic limit results for thick targets. For thin targets, membrane-based analytical models were demonstrated to be more suitable for targets undergoing only bulging. The shear plugging ratio is the only ballistic parameter required for the new analytical model. This parameter can easily be characterised from ballistic tests (post-test inspection of the target) or as will be shown in Chapter 5, can be accurately predicted numerically.

Together with the membrane models, the new two-stage analytical model allows for successful prediction of ballistic limit for any thickness against FSP. As well as providing a very good approximation of the ballistic performance, the models also validate the observed penetration and failure mechanisms of UHMW-PE composite under ballistic impact.
Chapter 5

Numerical Modelling of UHMW-PE Composite under Ballistic Impact

The previous chapter presented a new analytical model for predicting the ballistic limit performance of thick UHMW-PE composite against blunt projectiles. While this model captures the governing mechanisms and allows performance prediction in a limited number of cases, UHMW-PE composite can be used to protect against a wide range of projectiles in a multi-armour array with other armour materials. Only numerical models can provide the flexibility for analysis of a wide range of conditions. Numerical models also provide valuable data about the impact event that can not easily be measured experimentally, providing further insight into the phenomena. However the literature review showed that there are no modelling approaches that allow for accurate and efficient prediction of ballistic performance for thick UHMW-PE composite impacted between 900 m/s to 2000 m/s. This velocity regime is important because it represents many existing and emerging ballistic threats.

The work in this chapter builds on the findings of the evaluation study in the literature review. This study showed the current state-of-the-art modelling approach (non-linear orthotropic model) had deficiencies that led to poor ballistic performance prediction for thick targets. These shortcomings are addressed in this chapter. A sub-laminate approach to discretise the target is introduced to overcome inherent problems in the failure model and a new element erosion model more suitable for anisotropic materials is developed and implemented. The material data set is also updated to reflect results from more accurate material characterisation tests. The model is extensively validated against the large amount of experimental ballistic impact data reported in Chapter 3. Validation includes ballistic limit ($V_{50}$) and projectile residual velocity predictions for UHMW-PE composite targets up to 100 mm thick against 12.7 mm and 20 mm FSPs, as well as depth of penetration of semi-infinite UHMW-PE composite targets against 20 mm FSPs. Quantitative validation of the model is also made in terms of the target bulge geometry and apex and their dependence on the FSP velocity profile, and the plastic hinge position of the bulge in the composite.

The model is used to gain further insight into the ballistic impact response of thick UHMW-PE composite. Mechanisms of ballistic impact behaviour such as transition from shear plugging to bulging and delamination failure, which were difficult to elucidate experimentally, are investigated numerically in this chapter. The effect of fibre bridging and thermal softening on the ballistic performance of UHMW-PE composite is also investigated numerically. Finally a parametric study is performed to understand the sensitivity of the model to various changes to the material mechanical properties and to identify the key parameters that influence ballistic
The work in this chapter has been published in the following peer-reviewed journal publication:


5.1 Ballistic Impact Model

5.1.1 UHMW-PE Material Model

The non-linear orthotropic material model developed previously (Hayhurst et al., 1999; Clegg et al., 2006; Riedel et al., 2006; Wicklein et al., 2008) and implemented in ANSYS® AUTODYN® is used to model the ballistic impact response of UHMW-PE composite. The material model includes orthotropic coupling of the material volumetric and deviatoric response, non-linear equation of state, orthotropic hardening, stress-based composite failure criteria, and orthotropic energy-based softening. The material parameters for the UHMW-PE composite (Dyneema® HB26) that were used are detailed in Table 5.1. The model is described below with more detail also available in Clegg et al. (2006), Riedel et al. (2006), Wicklein et al. (2008), Lässig et al. (2015) and ANSYS (2013b).
where $\sigma_{ij}$ is the total stress, $s_{ij}$ is the deviatoric stress component, $P$ is the pressure and $\delta_{ij}$ is the Kronecker delta function. For all hydrocodes the total stress is computed from the strain field, which is represented in matrix form for a material with orthotropic properties as (Anderson

$$\sigma_{ij} = s_{ij} + P\delta_{ij}$$ (5.1)
et al., 1994):

\[
\begin{bmatrix}
\sigma_{xx} \\
\sigma_{yy} \\
\sigma_{zz} \\
\sigma_{xy} \\
\sigma_{xz} \\
\sigma_{yz}
\end{bmatrix}
= \begin{bmatrix}
E_x E_y E_z & E_x E_y E_z & E_x E_y E_z & 0 & 0 & 0 \\
E_x E_y E_z & E_x E_y E_z & E_x E_y E_z & 0 & 0 & 0 \\
E_x E_y E_z & E_x E_y E_z & E_x E_y E_z & 0 & 0 & 0 \\
E_x E_y E_z & E_x E_y E_z & E_x E_y E_z & 0 & 0 & 0 \\
E_x E_y E_z & E_x E_y E_z & E_x E_y E_z & 0 & 0 & 0 \\
E_x E_y E_z & E_x E_y E_z & E_x E_y E_z & 0 & 0 & 0 \\
\end{bmatrix}
\begin{bmatrix}
\varepsilon_{xx} \\
\varepsilon_{yy} \\
\varepsilon_{zz} \\
\varepsilon_{xy} \\
\varepsilon_{xz} \\
\varepsilon_{yz}
\end{bmatrix}
\]

(5.2)

\[
\Delta = 1 - \nu_{yx} \nu_{yx} - \nu_{yz} \nu_{yz} - \nu_{zx} \nu_{zx} - 2 \nu_{yx} \nu_{yz} \nu_{xz}
\]

(5.3)

However, anisotropic materials exhibit coupling of these two responses, i.e. strains are not uniform in all three material directions under hydrostatic pressure, and hydrostatic stresses lead to deviatoric strains and verse visa. Anderson et al. (1994) proposed a constitutive formulation for anisotropic material which allows the use of the theory of shock waves with coupling of the volumetric and deviatoric responses in the elastic regime. To do this, the total strain is decomposed into deviatoric and volumetric components:

\[
\varepsilon_{ij} = \varepsilon_{ij}^d - \frac{1}{3} \varepsilon_{vol} \delta_{ij}
\]

(5.4)

where \( \varepsilon_{ij} \) is the total strain, \( \varepsilon_{ij}^d \) is the deviatoric strain component and \( \varepsilon_{vol} \) is the volumetric strain (\( \varepsilon_{vol} = \nu/v_0 - 1 = \rho_0/\rho - 1 \)). The stresses are therefore a component of the deviatoric and volumetric strain components. For the linear orthotropic case, the stresses are as follows:

\[
\sigma_{xx} = \frac{1}{3} \left( C_{11} + C_{12} + C_{13} \right) \varepsilon_{vol} + C_{11} \varepsilon_{xx}^d + C_{12} \varepsilon_{yy}^d + C_{13} \varepsilon_{zz}^d
\]

(5.5)

\[
\sigma_{yy} = \frac{1}{3} \left( C_{21} + C_{22} + C_{23} \right) \varepsilon_{vol} + C_{21} \varepsilon_{xx}^d + C_{22} \varepsilon_{yy}^d + C_{33} \varepsilon_{zz}^d
\]

(5.6)

\[
\sigma_{zz} = \frac{1}{3} \left( C_{31} + C_{32} + C_{33} \right) \varepsilon_{vol} + C_{31} \varepsilon_{xx}^d + C_{32} \varepsilon_{yy}^d + C_{33} \varepsilon_{zz}^d
\]

(5.7)

\[
\sigma_{xy} = 2 C_{14} \varepsilon_{xy}^d
\]

(5.8)

\[
\sigma_{xz} = 2 C_{15} \varepsilon_{xz}^d
\]

(5.9)

\[
\sigma_{yz} = 2 C_{16} \varepsilon_{yz}^d
\]

(5.10)

The pressure is defined by the negative of the mean stress:

\[
P = -\frac{1}{3} \left( \sigma_{xx} + \sigma_{yy} + \sigma_{zz} \right)
\]

(5.11)
So that the pressure for a linear orthotropic material is given by:

\[ P = -\frac{1}{9} [C_{11} + C_{22} + C_{33} + 2(C_{12} + C_{23} + C_{31})] \varepsilon_{\text{vol}} \]

\[ -\frac{1}{3} (C_{11} + C_{21} + C_{31}) \varepsilon_{11}^d - \frac{1}{3} (C_{12} + C_{22} + C_{32}) \varepsilon_{22}^d - \frac{1}{3} (C_{13} + C_{23} + C_{33}) \varepsilon_{33}^d \]  

(5.12)

The first term in the above equation describes the pressure contribution from the volumetric component (linear EoS), while the latter three terms describe the pressure contribution from the deviatoric strain component. In Anderson et al. (1994), the first term is replaced with a general pressure term to describe non-linear shock effects (non-linear EoS):

\[ P = P(\varepsilon_{\text{vol}}, e) - \frac{1}{3} (C_{11} + C_{21} + C_{31}) \varepsilon_{11}^d - \frac{1}{3} (C_{12} + C_{22} + C_{32}) \varepsilon_{22}^d - \frac{1}{3} (C_{13} + C_{23} + C_{33}) \varepsilon_{33}^d \]

(5.13)

**5.1.1.2 Equation of State**

For non-linear shock effects, the pressure contribution from the volumetric strain \( P(\varepsilon_{\text{vol}}, e) \) is described using the Mie-Grüneisen EoS as proposed by Anderson et al. (1994):

\[ P(\varepsilon_{\text{vol}}, e) = P_r(v) + \Gamma(v) \left[ e - e_r(v) \right] \]

(5.14)

where \( v \) is the volume, \( e \) is the internal energy and \( \Gamma(v) \) is the Grüneisen coefficient. \( P_r(v) \) and \( e_r(v) \) refer to a reference pressure and internal energy, respectively. The shock Hugoniot is typically used as a reference condition. The shock formulation of the Mie-Grüneisen EoS is applied here, where an empirical linear relationship defines the shock and particle velocity relationship (discussed in more detail in section 2.4.4 of the literature review):

\[ U_s = c_0 + Su_p \]

(5.15)

where \( U_s \) is the shock wave velocity, \( c_0 \) is the bulk sound speed, \( S \) is the slope of the shock-particle velocity curve, and \( u_p \) is the particle velocity. The reference pressure, density and internal energy are then calculated from the Rankine-Hugoniot equations. Conditions off the Hugoniot reference curve are approximated with the Grüneisen coefficient \( \Gamma(v) \) from the second term in Eq. 5.14.

In the non-linear orthotropic model, \( c_0 \) is calculated from the elastic orthotropic constants and the slope of the \( U_s-u_p \) relationship, \( S \), is empirically adjusted to match flyer plate impact test results. The shock response of UHMW-PE composite has previously been shown by Hazell et al. (2011) to be similar to that of polyethylene, and therefore it is assumed the off-Hugoniot response of UHMW-PE composite is also similar. Thus, a Grüneisen coefficient for polyethylene of 1.64 is used in this work (Los Alamos, 1969).

One-dimensional simulations were performed using the model to validate the EoS against experimental inverse flyer plate impact tests performed in Lässig et al. (2015). The projectile was modelled with a 5 mm thick steel C-45 plate joined to a 6 mm thick UHMW-PE composite plate impacting a 2 mm thick steel C-45 plate as given in Lässig et al. (2015). Symmetry boundary conditions were imposed on the top and bottom nodes and a gauge was placed on the back of the witness plate to record the free surface velocity. The model is shown in Figure 5.1.
A comparison of the free surface velocity results is plotted in Figure 5.2 demonstrating good agreement of the initial and subsequent release waves. Free surface velocity measurements in inverse flyer plate impact tests are considered one-dimensional as long as the stress waves propagating from the lateral edges have not reached the specimen centre. In these tests the flyer plate is a 50 mm diameter disk, thus the one-dimensional strain assumption holds true until the impact release wave can travel from the outer edge of the flyer plate to the centre (i.e. half the diameter), where the free surface velocity is measured using VISAR. The in-plane wave speed of UHMW-PE composite is approximated by:

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

where \( E_f \) is the fibre elastic modulus (130 GPa), \( \rho_f \) is the fibre density (970 kg/m\(^3\)) and \( v_f \) is the composite fibre volume fraction (80%). A factor of \( \frac{1}{2} \) 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 C-45 witness plate. As shown in Figure 5.2, 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 ns to 3500 ns after impact the experimental and numerical results deviate as the experimental conditions can no longer be considered one-dimensional. The numerical curves have been produced through empirical adjustment of the \( S \) parameter, to correctly capture the non-linear shock compressibility.
5.1.1.3 Strength Model

The quadratic yield surface proposed by Chen et al. (1997) is used to describe non-linear, irreversible hardening of the material:

\[
f(\sigma_{ij}) = a_{11}\sigma_{11}^2 + a_{22}\sigma_{22}^2 + a_{33}\sigma_{33}^2 + 2a_{12}\sigma_{11}\sigma_{22} + 2a_{23}\sigma_{22}\sigma_{33} + 2a_{13}\sigma_{11}\sigma_{33} + 2a_{44}\sigma_{22}^2 + 2a_{55}\sigma_{33}^2 + 2a_{66}\sigma_{12}^2 = k
\]

where the nine plasticity coefficients, \(a_{ij}\), represent the degree of plastic anisotropy of the material, \(\sigma_{ij}\) are stresses in the principal material directions, and \(k\) is a state variable that defines the current limit of the yield surface. To describe strain hardening, \(k\) is replaced by a master effective stress-effective plastic strain curve, defined by 10 piecewise points in the hydrocode. It allows the determination of the stress states in any orthotropic direction from the plasticity coefficients \(a_{ij}\). The master effective stress \(\bar{\sigma}\) and master effective plastic strain \(\bar{\varepsilon}^p\) in the normal direction is defined by:

\[
\bar{\sigma} = \bar{\sigma}_{ii} \sqrt{\frac{3a_{ii}}{2}} \quad \text{and} \quad \bar{\varepsilon}^p = \bar{\varepsilon}_{ii} \sqrt{\frac{2}{3a_{ii}}}
\]

and in the shear direction by:

\[
\sigma = \bar{\sigma}_{ij} \sqrt{3a_{ii}} \quad \text{and} \quad \bar{\varepsilon}^p = \frac{\bar{\varepsilon}_{ij}}{\sqrt{3a_{ij}}}
\]

The master effective stress-effective plastic strain curve is best defined in the plane that experiences the largest non-linear plastic deformation. In the present work the in-plane shear stress-strain curve is used, and the corresponding plasticity coefficient, \(a_{44}\), is set to one. The other plasticity coefficients are set where possible to match experimental stress-strain curves.

![Figure 5.2: Free surface velocity trace from inverse flyer plate impact test and numerical simulation](image-url)
for the other orthotropic directions. Although the model proposed by Chen et al. (1997) is non-phenomenological, the model provides enough flexibility for the hardening behaviour in the other directions to be adequately described for this work. The yield surface produced using this model is shown in Figure 5.3 in the normal and shear stress planes.

![Figure 5.3: Yield surface for UHMW-PE composite in the (a) normal and (b) shear plane](image)

### 5.1.1.4 Failure Model

Failure in the non-linear orthotropic model is based on a combined stress criterion and is initiated when:

\[
\left( \frac{\sigma_{ii}}{S_{ii} (1 - D_{ii})} \right)^2 + \left( \frac{\sigma_{ij}}{S_{ij} (1 - D_{ij})} \right)^2 + \left( \frac{\sigma_{ki}}{S_{ki} (1 - D_{ki})} \right)^2 \geq 1 \quad \text{for} \quad i, j, k = 1, 2, 3 \quad (5.20)
\]

where \( S_{ii} \) is the failure strength of the material in the respective directions. The damage parameter, \( D_{ii} \), follows a linear relationship with stress and strain, shown in Figure 5.4 and is defined by:

\[
D_{ii} = \frac{L \sigma f \varepsilon_{cr}}{2G_{f,ii}} \quad (5.21)
\]

where \( L \) is the characteristic cell length, \( \varepsilon_{cr} \) is the crack strain (strain above the failure initiation strain) and \( G_{f,ii} \), the fracture energy in the direction of damage.
The strength and failure model is verified against mechanical test results for UHMW-PE composite (Dyneema® HB26) under in-plane tension, out-of-plane compression, and in-plane and out-of-plane shear from a number of different sources (Lässig (2012), Heisserer (2013), Chocron et al. (2014) and Lässig et al. (2015)). This analysis was not performed for out-of-plane tension because this is described at the sub-laminate interface (section 5.1.3.2). In-plane compression was also not considered because it is not important in the penetration resistance of the material (Greenhalgh et al., 2013). The numerical verification involves simulation of a single element under the appropriate loading and boundary conditions, the results of which are shown in Figure 5.5. Single element models are used for this purpose as opposed to full specimens to avoid stress wave oscillations across multiple elements, which prevent full specimens from reaching equilibrium.

Figure 5.4: Crack softening law
Figure 5.5: Stress-strain curves from single element simulations compared to experiments. (a) in-plane tension, (b) in-plane shear, (c) out-of-plane shear and (d) out-of-plane compression.

The numerical model is shown to provide good agreement for the in-plane tension and reasonable agreement for the out-of-plane compression simulation. The in-plane shear simulation is also in good agreement; however failure occurs at a higher stress in the numerical model. The experimental curve is from a 45° in-plane tension test, for which failure measurements are known to be dependent on specimen geometry (Kellas et al., 1993). For example, Lässig (2012) measured failure strengths between 35 MPa and 55 MPa for a UHMW-PE specimen with a 20 mm gauge width tested at different loading rates. Experiments performed by Heisserer (2013) on the same material with a gauge width of 80 mm gave failure stresses greater than 120 MPa. Since validation of the model is ultimately performed on ballistic panels with lateral dimensions greater than 300 mm, the larger value is taken.

The agreement for out-of-plane shear stress-strain is not as good, yet still reasonable (Figure 5.5(c)). This is due to the simplification of the strength model, which describes all stress states in the principal directions as scalable with respect to the master effective stress-effective plastic strain curve by one single plasticity constant per loading mode. In through-thickness shear experiments conducted by Lässig et al. (2015), measurements were not made for shear strains greater than 0.4, at which point the specimen had not completely failed. As the specimen is loaded in shear, the shear deformation causes the fibres to become more aligned with the diagonal tension. This behaviour is more prominent for UHMW-PE composites because of the large difference between the fibre and matrix properties and results in a significant increase in
the slope (as seen in Lässig et al. (2015)), and is replicated in the model.

Under this loading condition complete failure occurs due to either fibre tensile failure as a result of fibre realignment, or fibre shearing. As such, the principal stress is used to determine the maximum out-of-plane failure shear stress for the specimen under uniaxial in-plane tension:

\[ S_{12,31} = \frac{S_{22,33}}{2} \]  

(5.22)

This is consistent with the approach used in the analytical model chapter to determine the through-thickness shear strength. This provides a through-thickness shear strength value of 575 MPa which is the same as the value in the analytical model.

### 5.1.2 Target Erosion Model

When modelling ballistic impact events using Lagrangian discretisation, large element distortion occurs that affects time step and numerical stability. Erosion models based on effective strain have been used to successfully overcome this issue for isotropic materials (Chocron et al., 2001), where the effective strain is defined as (ANSYS, 2013a):

\[
\varepsilon_{\text{eff}} = \frac{2}{3} \left[ (\varepsilon_{11}^2 + \varepsilon_{22}^2 + \varepsilon_{33}^2) - (\varepsilon_{11}\varepsilon_{22} + \varepsilon_{22}\varepsilon_{33} + \varepsilon_{33}\varepsilon_{11}) + 3 \left( \varepsilon_{12}^2 + \varepsilon_{23}^2 + \varepsilon_{31}^2 \right) \right]^{1/2}
\]  

(5.23)

This formulation was shown in the benchmarking study in the literature review to be inappropriate for anisotropic materials because the failure strains can be substantially different in different directions due to the directionally-dependent nature of fibre-reinforced composites. When failure occurs in one direction, the corresponding stiffness in that direction is set to zero. This leads to large strains in that direction, which in the case of impact and penetration can be excessive as the driving force of the impactor continues to expand the element. If the strains in the other directions remain small, then the effective strain does not capture these extreme strains as the calculation has an averaging effect. Furthermore, due to the continuum definition, as the strains increase significantly in one direction following failure, the overall volume of the element increases. This has the effect of artificially preventing failure in the other directions, as forces in these directions generate lower stresses due to the increased volume. This effect becomes more pronounced at the excessive strains seen in penetration simulations.

A new damage-based erosion model was written in a user subroutine to address this problem (Appendix A). In this model, elements are eroded only when they are fully damaged in the in-plane (fibre) direction, which is defined by:

\[ D_{22} = 1 \]  

(5.24)

\[ D_{33} = 1 \]  

(5.25)

An out-of-plane criterion (e.g. in the 11 direction) is not included because out-of-plane failure, is captured separately which will be discussed in a latter section. A global instantaneous erosion strain of 150% was also applied to delete any highly distorted elements that affect the time step and numerical stability without playing a further role on the target interaction.

Figure 5.6 shows the velocity profile of simulations computed using the effective strain criterion (IGS, or instantaneous geometric strain in AUTODYN®) with an effective strain of 250% (IGS2.5) and 150% (IGS1.5), in comparison with simulation results using the damage-based
criterion (DMG). Cross-sections from the IGS2.5 numerical analysis at two points, A and B, are shown with contours of in-plane material damage. Experimental results are also shown, where the residual velocity and perforation time are indicated together with shaded regions corresponding to the typical experimental variance. The results show that using the IGS erosion model with an effective strain of 250%, at 30 μs after impact (point A), the model has predicted complete failure through the thickness of the target. However, the projectile velocity continues to reduce beyond this point in time. At 200 μs after impact (point B), the failed elements have elongated significantly but the projectile velocity still continues to decrease. In reality, this failed material would no longer be influencing the projectile. The unrealistic behaviour is an artefact of the strain-based erosion model for an anisotropic material. Decreasing the effective strain to 150% reduces the extent of this unrealistic behaviour, but does not eliminate it completely. In contrast, the damage-based erosion model gives accurate predictions of the residual velocity and perforation time, in addition to more realistic representations of the material performance as shown at point C in Figure 5.6.

Figure 5.6: Projectile velocity versus time from impact for 10 mm thick targets impacted by 20 mm FSP at 469 m/s. Right images are cross-section contours through the model mid-plane from the IGS2.5 configuration, corresponding to points on the velocity profiles, where red indicates elements that have failed in the in-plane direction and blue indicates no in-plane failure.

5.1.3 Target Model

The ballistic limit tests described in Chapter 3 used targets measuring 300 mm × 300 mm except for the 76 mm and 100 mm thick panels which had lateral dimensions of 400 mm × 400 mm. The full target is modelled (i.e. no symmetry) because the material is orthotropic and the projectile shape is asymmetric. In ANSYS® AUTODYN®, the out-of-plane direction is designated as the 11 material direction and the in-plane directions are the 22 and 33 directions. The model used 8-node hexahedral elements to mesh the projectile and target. No boundary conditions were imposed on the target because high speed video of ballistic impact tests typically showed clamp slippage upon impact due to the low friction coefficient of UHMW-PE composite.

The evaluation of the non-linear orthotropic model in the literature review chapter showed that
the model was prone to predict premature through-thickness shear failure because through-
thickness shear failure was coupled to through-thickness tensile failure (which comparatively
has very low strength). The premature initiation of failure in the out-of-plane direction caused
an under-prediction of the ballistic limit velocity. To overcome this problem, a sub-laminate
approach was used to discretise the target as opposed to the traditional monolithic discreti-
sation approach. Sub-laminate discretisation of the target involves dividing the target into
sub-laminates (which still have the orthotropic properties of the laminate), with multiple sub-
 laminates stacked to form the target. With this approach the through-thickness tensile failure
can be decoupled from the through-thickness shear failure in the sub-laminates by disabling
out-of-plane tensile failure (by setting the failure stress to a very high value). The sub-laminate
interface is bonded and a stress-based failure criterion is set to allow out-of-plane failure be-
tween the sub-laminate interfaces (discussed more in a latter section). A comparison between
monolithic and sub-laminate target discretisation is shown in Figure 5.7.

The sub-laminates are one element thick and are separated by a small gap. This gap was 0.06
mm for targets impacted by 12.7 mm FSP or 0.1 mm for targets impacted by 20 mm FSP to
satisfy the master-slave contact (external gap) algorithm used to detect contact between the
different bodies. A gapless contact algorithm was also investigated, however this resulted in
over-penetration of elements leading to poorer predictions, and was therefore not used. While
the addition of these gaps increased the total thickness of the target, the actual thickness of the
target was only considered to be occupied by material, i.e. the sum of the thickness of all of the
sub-laminates. The sum of all the gaps was small (\(<5\%\)) compared to the total thickness of the
target and was considered to have negligible effects on the ballistic performance.

Figure 5.7: Target discretisation (a) monolithic and (b) sub-laminate
5.1.3.1 Mesh Refinement Study

Influence of in-plane mesh size on penetration resistance

A mesh refinement study was performed for a DoP test to determine the in-plane element length in the penetration zone. The DoP case was chosen to remove the bulging failure mode (which can be driven by the sub-laminate thickness). The element dimensions of the target sub-laminates were approximately uniform (aspect ratio of 1) and equal to the projectile mesh size at the impact site. After extending to a radius of one projectile diameter from the penetration zone, the mesh size was radially graded. The mesh size was controlled by the number of elements across the projectile diameter, and the need to maintain the same element size between the projectile and the target penetration zone. Matching the projectile and target mesh is critical in order to avoid a stiffness mismatch due to the relative mesh size of the target and projectile. Table 5.2 shows the cases considered and the resulting number of sub-laminates in the target, the in-plane element length around penetration zone or sub-laminate thickness, the predicted depth of penetration, and the difference (% Diff) relative to the case with the finest mesh. The projectile velocity profile is also important to consider for this mesh refinement study and is shown in Figure 5.8 for the different cases.

Table 5.2: Mesh refinement study results on 100 mm DoP target impacted by 20 mm FSP at 1140 m/s

<table>
<thead>
<tr>
<th>Elements across projectile diameter</th>
<th>No. of sub-laminates</th>
<th>In-plane element length or sub-laminate thickness (mm)</th>
<th>DoP (mm)</th>
<th>% Diff</th>
</tr>
</thead>
<tbody>
<tr>
<td>6</td>
<td>30</td>
<td>3.3</td>
<td>46.6</td>
<td>-14.5</td>
</tr>
<tr>
<td>9</td>
<td>50</td>
<td>2.0</td>
<td>52.0</td>
<td>-4.6</td>
</tr>
<tr>
<td>12</td>
<td>60</td>
<td>1.7</td>
<td>53.3</td>
<td>-2.2</td>
</tr>
<tr>
<td>15</td>
<td>75</td>
<td>1.3</td>
<td>54.5</td>
<td></td>
</tr>
</tbody>
</table>

Figure 5.8: Projectile velocity profile results from mesh refinement study on 100 mm DoP target impacted by 20 mm FSP at 1140 m/s
In this study, satisfactory convergence was reached for cases where there were 9 or more elements across the projectile diameter, which corresponds to an in-plane element length around the penetration zone and sub-laminate thickness of 2 mm. Predicted penetration depth for this case is within 5% of the finest mesh and the projectile velocity profile is reasonably converged.

**Influence of sub-laminate thickness on ballistic performance and shear plugging ratio**

In order to study the effect of mesh size on the performance and bulging of the target, finite thickness targets were studied. In this study, a 20 mm target impacted by a 20 mm FSP was modelled with sub-laminates having the same in-plane elements length found in the DoP mesh study (2 mm). The out-of-plane element dimension or sub-laminate thickness was varied, and the influence on ballistic performance (measured in terms of the projectile residual velocity) and shear plugging ratio was investigated. Different impact regimes were studied including under-match, slightly over-match and large over-match of the target because UHMW-PE composite exhibits different proportions of shear plugging and bulging failure mode depending on the impact velocity.

The results of this study is shown in Table 5.3. For the case where the target is under-matched (impact velocity of 605 m/s), the models using 10 or more sub-laminates (sub-laminate thickness smaller than 2 mm) resulted in the projectile being stopped. Models with coarser mesh reported a complete penetration result with reasonably consistent projectile residual velocities. There is a degree of scatter in the shear plugging ratio for this case with the difference between the highest and lowest values of 38%, which is about the contribution of a single sub-laminate from the coarsest mesh. This is reduced to 15% for the cases where the projectile was stopped. For the slightly over-matched case (impact velocity of 684 m/s), all cases resulted in complete penetration with some scatter in the projectile residual velocity. The shear plugging ratio is more consistent for the three finest mesh (within 5%) whereas the three thickest sub-laminate mesh have a high degree of scatter (difference of 53%). For the slightly over-matched cases, a coarse sub-laminate discretisation leads to poor prediction of the shear plugging ratio. For the large over-matched case (impact velocity of 1000 m/s) the projectile residual velocity and shear plugging ratio are reasonably consistent across all sub-laminate thickness. The shear plugging ratio for the target with only 3 sub-laminates was not able to capture intermediate shear plugging ratios between 33% to 67%, leading to large differences in the results compared to targets with more sub-laminates.
Table 5.3: Mesh refinement results on 20 mm thick target impacted by 20 mm FSP. Experimental $V_{50}$ for this configuration is 620 m/s

<table>
<thead>
<tr>
<th>Impact regime</th>
<th>Sub-laminate thickness (mm)</th>
<th>No. of sub-laminates</th>
<th>$V_I$ (m/s)</th>
<th>$V_R$ (m/s)</th>
<th>$t_S/t$</th>
</tr>
</thead>
<tbody>
<tr>
<td>under match</td>
<td>1.00</td>
<td>20</td>
<td>605</td>
<td>0</td>
<td>0.35</td>
</tr>
<tr>
<td></td>
<td>1.33</td>
<td>15</td>
<td>605</td>
<td>0</td>
<td>0.40</td>
</tr>
<tr>
<td></td>
<td>2.00</td>
<td>10</td>
<td>605</td>
<td>0</td>
<td>0.50</td>
</tr>
<tr>
<td></td>
<td>2.86</td>
<td>7</td>
<td>605</td>
<td>437</td>
<td>0.29</td>
</tr>
<tr>
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<td>4.00</td>
<td>5</td>
<td>605</td>
<td>438</td>
<td>0.40</td>
</tr>
<tr>
<td></td>
<td>6.00</td>
<td>3</td>
<td>605</td>
<td>427</td>
<td>0.67</td>
</tr>
<tr>
<td>slightly over-match</td>
<td>1.00</td>
<td>20</td>
<td>684</td>
<td>376</td>
<td>0.45</td>
</tr>
<tr>
<td></td>
<td>1.33</td>
<td>15</td>
<td>684</td>
<td>238</td>
<td>0.47</td>
</tr>
<tr>
<td></td>
<td>2.00</td>
<td>10</td>
<td>684</td>
<td>364</td>
<td>0.50</td>
</tr>
<tr>
<td></td>
<td>2.86</td>
<td>7</td>
<td>684</td>
<td>493</td>
<td>0.86</td>
</tr>
<tr>
<td></td>
<td>4.00</td>
<td>5</td>
<td>684</td>
<td>513</td>
<td>0.40</td>
</tr>
<tr>
<td></td>
<td>6.00</td>
<td>3</td>
<td>684</td>
<td>516</td>
<td>0.33</td>
</tr>
<tr>
<td>large over-match</td>
<td>1.00</td>
<td>20</td>
<td>1000</td>
<td>827</td>
<td>0.50</td>
</tr>
<tr>
<td></td>
<td>1.33</td>
<td>15</td>
<td>1000</td>
<td>791</td>
<td>0.53</td>
</tr>
<tr>
<td></td>
<td>2.00</td>
<td>10</td>
<td>1000</td>
<td>803</td>
<td>0.60</td>
</tr>
<tr>
<td></td>
<td>2.86</td>
<td>7</td>
<td>1000</td>
<td>782</td>
<td>0.43</td>
</tr>
<tr>
<td></td>
<td>4.00</td>
<td>5</td>
<td>1000</td>
<td>805</td>
<td>0.60</td>
</tr>
<tr>
<td></td>
<td>6.67</td>
<td>3</td>
<td>1000</td>
<td>825</td>
<td>0.67</td>
</tr>
</tbody>
</table>

Mesh Selection

The mesh refinement study was broken into two sections; the first where the penetration resistance of element was studied using DoP models and a second section where the sub-laminate thickness and its influence on performance and shear plugging ratio on a finite thickness target was investigated. The first study showed convergence in penetration resistance when the in-plane element length around the penetration zone was about $1/9^{th}$ (or smaller) of the projectile diameter. The second study showed targets meshed with sub-laminates thicker than 2 mm tend to under-predict performance close to the ballistic limit. Furthermore, targets meshed with only 3 sub-laminate were found to be insufficient in capturing the shear plugging ratio, even considering the large scatter typically observed with this type of measurement.

For simplicity and consistency, all targets will be modelled with the in-plane element length at the penetration zone based on $1/9^{th}$ of the projectile diameter. The sub-laminate thickness is set to the same size to maintain element aspect ratio of one in the penetration zone unless the number of sub-laminates in the target reduces to 3 or less (not the case for the targets investigated in this work). The in-plane element length or sub-laminate thickness is 1.4 mm and 2.0 mm for targets impacted by 12.7 mm FSP and 20 mm FSP respectively. Table 5.4 shows the in-plane element size or sub-laminate thickness and the total number of sub-laminates in the target for each configuration considered.
Table 5.4: In-plane element length or sub-laminate thickness and number of sub-laminates for different target configurations

<table>
<thead>
<tr>
<th>Target thickness (mm)</th>
<th>Projectile</th>
<th>In-plane element length or sub-laminate thickness (mm)</th>
<th>No. of sub-laminates</th>
</tr>
</thead>
<tbody>
<tr>
<td>9.1</td>
<td>12.7 mm FSP</td>
<td>1.4</td>
<td>7</td>
</tr>
<tr>
<td>20</td>
<td>12.7 mm FSP</td>
<td>1.4</td>
<td>14</td>
</tr>
<tr>
<td>25.2</td>
<td>12.7 mm FSP</td>
<td>1.4</td>
<td>18</td>
</tr>
<tr>
<td>35.1</td>
<td>12.7 mm FSP</td>
<td>1.4</td>
<td>25</td>
</tr>
<tr>
<td>50.4</td>
<td>12.7 mm FSP</td>
<td>1.4</td>
<td>36</td>
</tr>
<tr>
<td>10</td>
<td>20 mm FSP</td>
<td>2</td>
<td>5</td>
</tr>
<tr>
<td>20</td>
<td>20 mm FSP</td>
<td>2</td>
<td>10</td>
</tr>
<tr>
<td>36.2</td>
<td>20 mm FSP</td>
<td>2</td>
<td>18</td>
</tr>
<tr>
<td>75.6</td>
<td>20 mm FSP</td>
<td>2</td>
<td>38</td>
</tr>
<tr>
<td>101.7</td>
<td>20 mm FSP</td>
<td>2</td>
<td>51</td>
</tr>
</tbody>
</table>

5.1.3.2 Sub-laminate Interface

The sub-laminates were kinematically joined together using a bonded contact that was breakable through a criterion combining normal and shear stresses:

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

(5.26)

where \(\sigma\) and \(S\) are stress and strength values, subscripts \(N\) and \(S\) are the normal and shear directions respectively, and the exponents \(a\) and \(b\) were assumed to be 1.0 due to the absence of combined loading data. It was shown in Chapter 3 that under ballistic impact, interlaminar failure and the development of delamination and transition planes is driven by the propagation of the impact pressure wave and the interaction of release waves. This failure mode occurs under high strain rates and pressure and is dominated by the matrix material, which has been shown to be highly strain-rate and pressure-dependent (Qi and Boyce (2005) and Chocron et al. (2014)). As such, high strain rate and/or high pressure values for through-thickness tension and interlaminar shear should be used for \(S_N\) and \(S_S\). However, no high strain rate experiments have been reported for UHMW-PE composite, which would allow a determination of the through-thickness tensile and shear strengths at ballistic conditions.

Dynamic spallation tests performed by Riedel et al. (2003b) on carbon and aramid fibre-reinforced composites have shown the through-thickness tensile strength to be between 2 to 5 times higher than quasi-static values. Based on this, the quasi-static through-thickness tensile strength value for UHMW-PE composite (1.07 MPa (Lässig et al., 2015)) was multiplied by a factor of 5, to determine an approximate high strain rate value. Even with the addition of this factor to account for high strain rate, the through-thickness failure strength used for UHMW-PE composite is still significantly lower that the dynamic spall strength of aramid-epoxy (95 MPa) and carbon-epoxy (250 MPa) composites as measured by Riedel et al. (2003b).

The interlaminar shear strength of UHMW-PE composite was characterised in Chocron et al. (2014) at 500 MPa of hydrostatic pressure. The increase in interlaminar shear strength due to hydrostatic pressure is shown in the literature review (Figure 2.13). The high pressure value determined is 3 times higher than the value measured under atmospheric conditions, as the initiation and propagation of cracks are inhibited under hydrostatic pressure (Vyas et al., 2011). The high pressure interlaminar shear strength was used in Chocron et al. (2014) to
model UHMW-PE composite under ballistic impact and will also be used in this work. As will be shown in the results section of this chapter, simulations show interlaminar failure occurs under high pressure around the profile of the pressure wave.

Unlike a cohesive element approach used in finite element analysis of interlaminar failure of composites (Mi et al., 1998), this stress-based approach is not able to take into account the energy required to propagate a crack after it has initiated. However, since the fracture energy of UHMW-PE composite is low (Lässig et al., 2015), interlaminar failure only accounts for a very small portion of the total energy absorbed under ballistic impact (Peijs et al., 1994). As such, the omission of this energy is considered to have a minor effect on the prediction of ballistic performance, although a larger predicted delamination area is expected in the model.

5.1.4 Projectile Model

The FSP is made from 4340H steel with a Rockwell C hardness of 30 (MIL-DTL-46593B (Department of Defense, 2006)). The material was described using the Johnson-Cook strength model (Johnson and Cook, 1983) and the shock formulation of the Mie-Grüneisen EoS; the parameters for which are given in Table 5.5.

Table 5.5: FSP material parameters

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Symbol</th>
<th>Value</th>
<th>Units</th>
<th>Source</th>
</tr>
</thead>
<tbody>
<tr>
<td>Density</td>
<td>( \rho )</td>
<td>7880</td>
<td>kg/m³</td>
<td>(Yen, 2012)</td>
</tr>
<tr>
<td>Grüneisen coefficient</td>
<td>( \Gamma )</td>
<td>2.17</td>
<td>-</td>
<td>(Los Alamos, 1969)</td>
</tr>
<tr>
<td>Parameter C1</td>
<td>( c_0 )</td>
<td>4.57×10³</td>
<td>m/s</td>
<td>(Los Alamos, 1969)</td>
</tr>
<tr>
<td>Parameter S1</td>
<td>( S )</td>
<td>1.49</td>
<td>-</td>
<td>(Los Alamos, 1969)</td>
</tr>
<tr>
<td>Reference Temperature</td>
<td>( T_0 )</td>
<td>300</td>
<td>k</td>
<td>-</td>
</tr>
<tr>
<td>Specific Heat</td>
<td>( c )</td>
<td>477</td>
<td>J/kgK</td>
<td>(Johnson and Cook, 1983)</td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Symbol</th>
<th>Value</th>
<th>Units</th>
<th>Source</th>
</tr>
</thead>
<tbody>
<tr>
<td>Strength: Johnson-Cook</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Shear Modulus</td>
<td>( G )</td>
<td>7.78×10⁷</td>
<td>kPa</td>
<td>(Yen, 2012)*</td>
</tr>
<tr>
<td>Yield Stress</td>
<td>( A )</td>
<td>1.03×10⁶</td>
<td>kPa</td>
<td>(Yen, 2012)</td>
</tr>
<tr>
<td>Hardening Constant</td>
<td>( B )</td>
<td>4.77×10⁵</td>
<td>kPa</td>
<td>(Johnson and Cook, 1983)</td>
</tr>
<tr>
<td>Hardening Exponent</td>
<td>( n )</td>
<td>0.18</td>
<td>-</td>
<td>(Johnson and Cook, 1983)</td>
</tr>
<tr>
<td>Strain Rate Constant</td>
<td>( C )</td>
<td>0.012</td>
<td>-</td>
<td>(Johnson and Cook, 1983)</td>
</tr>
<tr>
<td>Thermal Softening Exponent</td>
<td>( m )</td>
<td>1.0</td>
<td>-</td>
<td>(Johnson and Cook, 1983)</td>
</tr>
<tr>
<td>Melting Temperature</td>
<td>( T_M )</td>
<td>1763</td>
<td>K</td>
<td>(Johnson and Cook, 1983)</td>
</tr>
<tr>
<td>Ref. Strain Rate</td>
<td>( \dot{\varepsilon} )</td>
<td>1.0</td>
<td>s⁻¹</td>
<td>(Johnson and Cook, 1983)</td>
</tr>
</tbody>
</table>

*calculated from an elastic modulus of 207 GPa and Poisson ratio of 0.33

The Johnson-Cook strength model parameters for 4340H steel were based on those of S-7 steel (Johnson and Cook, 1983), with a reduction in the yield strength from 1590 MPa to 1030 MPa to reflect the reported yield stress of 4340H steel (Yen, 2012). The parameters for strain and strain-rate hardening were unchanged in absence of experimental data. Parameters for the shock EoS of steel were taken from Los Alamos (1969).
5.2 Results and Discussion

5.2.1 Depth of Penetration of Semi-Infinite Targets

The results of the depth of penetration for the semi-infinite UHMW-PE composite targets against 20 mm FSP reported in Chapter 3 is used as a first step to validate and assess the accuracy of the numerical model. Details of the test are in section 3.2 of Chapter 3. Experimentally and numerically, the DoP was measured from the difference between the original panel thickness and the remaining unperforated thickness after ballistic impact.

The sub-laminates had in-plane element lengths in the penetration zone and thickness of 2 mm, resulting in 50 and 75 sub-laminates for the 100 mm and 150 mm thick UHMW-PE targets respectively. Numerical simulations were performed with and without a steel backing, with the back face fixed from displacement in the direction of impact. No discernible difference in penetration depth was found for the two cases. The DoP results obtained by modelling and experimentation are given in Table 5.6, with excellent agreement between the experimental and numerical results.

Table 5.6: Depth of penetration results

<table>
<thead>
<tr>
<th>Target thickness (mm)</th>
<th>Impact velocity (m/s)</th>
<th>DoP Experiment (mm)</th>
<th>DoP Numerical (mm)</th>
<th>% Diff</th>
</tr>
</thead>
<tbody>
<tr>
<td>100</td>
<td>815</td>
<td>30.0</td>
<td>30</td>
<td>0.0</td>
</tr>
<tr>
<td>150</td>
<td>991</td>
<td>39.8</td>
<td>40</td>
<td>0.5</td>
</tr>
<tr>
<td>150</td>
<td>1038</td>
<td>45.2</td>
<td>44</td>
<td>-2.7</td>
</tr>
<tr>
<td>150</td>
<td>1305</td>
<td>61.4</td>
<td>60</td>
<td>-2.3</td>
</tr>
</tbody>
</table>

Figure 5.9 shows the post-impacted target from experiment and bonded contact contours of the numerical target and the target cross-section. Qualitatively, the perforated sub-laminates in the numerical model have mostly debonded from the adjacent sub-laminates except for the target edges. The numerical model predicts extensive delamination, which occurs over all sub-laminate interfaces, with the highest crack opening along the orthogonal impact planes as shown in the cross-section image in Figure 5.9(c). Delamination is more extensive in numerical model than compared to the experiment and is expected because the stress-based failure criterion used to fail the sub-laminate interface is not able to account for the additional energy in propagating the crack across the element. Despite this, the numerical depth of penetration predictions are very close to the experimental value and indicate that the extent of delamination failure is not critical for depth of penetration in semi-infinite targets.
Simulations were conducted to predict the ballistic performance of various thicknesses of UHMW-PE composite panels against 12.7 mm and 20 mm FSP. The details of the mesh and number of sub-laminates for each configuration is given in Table 5.4. The targets were impacted over a range of impact velocities requiring one partial penetration and at least three complete penetrations. The residual velocities of the FSPs following target perforation were fit to the Lambert-Jonas equation (Lambert and Jonas, 1976), Eq. 5.27, from which an estimate of the ballistic limit was determined:

$$V_R = a \left( V_I^p - V_{BL}^p \right)^{1/p}$$

where $V_R$ is the residual velocity, $V_I$ is the impact velocity and $V_{BL}$ is the ballistic limit velocity. $V_{BL}$, $a$ and $p$ are determined from a least squares fit of the impact and residual velocity results, from which an estimate of the ballistic limit velocity ($V_{BL}$) is determined. The results of the Lambert-Jonas analysis are shown in Figure 5.10. In the experimental program, the projectile residual velocity results for the 10 mm and 20 mm thick targets impacted by 20 mm FSPs were made using high speed photography. These measurements are plotted in Figure 5.10(b) for comparison. The numerical results show excellent agreement with the experimental residual velocity measurements.
Figure 5.10: Numerical residual velocity predictions for UHMW-PE composite impacted by (a) 12.7 mm FSP and (b) 20 mm FSP with comparison to experimental measurements. The numerical results are fit to the Lambert-Jonas equation, parameters for which are given in the legend (\(a, p\) and \(V_{BL}\)). The numerical ballistic limit predictions are compared to experimental \(V_{50}\) results in Figure 5.11. The graph is plotted for both 12.7 mm and 20 mm FSP against UHMW-PE target thicknesses from 10 mm to 100 mm with respect to non-dimensional areal density. For thinner targets, the results are in excellent agreement with the experimental results. The results deviate slightly with increasing thickness and impact speed (this discrepancy is addressed in section 5.2.3 of this chapter); however predictions are all within 20% of the experimental value and close to the experimental scatter range. A summary of the results showing the experimental \(V_{50}\), experimental standard deviation (\(\sigma_{V_{50}}\)) and coefficient of variation (\(c_v\)), numerical predicted ballistic limit (\(V_{BL}\)), and percentage difference (% Diff) between the calculated and measured limit values is given in Table 5.7. The \(V_{BL}\) range in Table 5.7 shows the range between the highest partial penetration and lowest complete penetration results from the model.
Figure 5.11: Experimental and numerical ballistic limit results plotted against the non-dimensional areal density

Table 5.7: Experimental and numerical ballistic limit results

<table>
<thead>
<tr>
<th>Target thickness (mm)</th>
<th>Threat</th>
<th>$V_{50}$ (m/s)</th>
<th>$\sigma_{V_{50}}$ (m/s)</th>
<th>$c_v$ (%)</th>
<th>$V_{BL}$ (m/s)</th>
<th>$V_{BL}$ range (m/s)</th>
<th>% Diff</th>
</tr>
</thead>
<tbody>
<tr>
<td>9.1</td>
<td>12.7 mm FSP</td>
<td>506</td>
<td>26</td>
<td>5.2</td>
<td>450</td>
<td>450-500</td>
<td>-11.1</td>
</tr>
<tr>
<td>20</td>
<td>12.7 mm FSP</td>
<td>826</td>
<td>17</td>
<td>2.1</td>
<td>984</td>
<td>900-1000</td>
<td>19.1</td>
</tr>
<tr>
<td>25.2</td>
<td>12.7 mm FSP</td>
<td>1021</td>
<td>8</td>
<td>0.</td>
<td>1096</td>
<td>1000-1100</td>
<td>7.3</td>
</tr>
<tr>
<td>35.1</td>
<td>12.7 mm FSP</td>
<td>1250</td>
<td>36</td>
<td>2.9</td>
<td>1387</td>
<td>1300-1400</td>
<td>11</td>
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<tr>
<td>50.4</td>
<td>12.7 mm FSP</td>
<td>1657</td>
<td>16</td>
<td>1.0</td>
<td>1800</td>
<td>1800-1900</td>
<td>8.6</td>
</tr>
<tr>
<td>10</td>
<td>20 mm FSP</td>
<td>394</td>
<td>43</td>
<td>10.9</td>
<td>394</td>
<td>394-440</td>
<td>0</td>
</tr>
<tr>
<td>20</td>
<td>20 mm FSP</td>
<td>620</td>
<td>20</td>
<td>3.2</td>
<td>626</td>
<td>605-684</td>
<td>1</td>
</tr>
<tr>
<td>36.2</td>
<td>20 mm FSP</td>
<td>901</td>
<td>10</td>
<td>1.1</td>
<td>1077</td>
<td>1020-1100</td>
<td>19.5</td>
</tr>
<tr>
<td>75.6</td>
<td>20 mm FSP</td>
<td>1528</td>
<td>105</td>
<td>6.9</td>
<td>1734</td>
<td>1660-1750</td>
<td>13.5</td>
</tr>
<tr>
<td>101.7</td>
<td>20 mm FSP</td>
<td>2002</td>
<td>92</td>
<td>4.6</td>
<td>2300</td>
<td>2300-2400</td>
<td>14.9</td>
</tr>
</tbody>
</table>

Figure 5.12 compares the deformation of a 102 mm thick target impacted at just under the ballistic limit by 20 mm FSP from experimental (post-impacted) and simulation (1 ms after impact). The deformation response of UHMW-PE composite is well predicted with key features including bulge deformation, formation of multiple sub-laminates, sub-laminate folding, material drawing etc. accurately captured by the model.
5.1.2.1 Delamination

Delamination failure is extensive in UHMW-PE composite under impact and is important for the deformation response of the target. The model is able to predict this failure mode where failure
between the bonds of adjacent sub-laminates are predicted. Figure 5.14 shows a simulation of a 102 mm thick target just below the ballistic limit velocity. Figure 5.14(a) shows the pressure wave produced ahead of the projectile 10 μs after initial impact. The amplitude of the pressure wave is very high in the order of $10^2$ MPa to $10^3$ MPa. Figure 5.14(b) shows the contour of sub-laminate bond failure as indicated in dark blue. Figure 5.14(c) provides an overlay of the pressure front on the bonded interface contours.

The result shows that interlaminar failure not only occurs under very high pressure but follows the contours of the pressure wave front. As such, by the time the pressure wave reaches the back, a significant portion of material ahead of the projectile is delaminated, reducing the resistance of target material ahead of the projectile to bending and allowing the rear portion of the target to easily undergo large bulge deformation. Figure 5.15 shows the bonded interface contours of the 102 mm thick target. The model predicts extensive delamination failure between the sub-laminate interface, dominated by the region along the fibre directions in the impact plane and the bulging section of the target. Although the region of delamination compares well with the post-impacted targets, the extent of delamination is less in experiment (i.e. delamination is over-predicted in the model). This is expected because the stress-based failure criterion at the sub-laminate interface does not account for the additional energy in propagating the delamination crack across the element. Furthermore, for these finite targets, mode I delamination can occur due to the formation of the bulge. However, interlaminar toughing mechanisms such as fibre bridging between sub-laminates is not modelled, causing the model to over-predict delamination failure. Despite this, the ballistic performance prediction and target deformation behaviour is still in good agreement with experiment.

Figure 5.14: 102 mm target 10 μs after initial impact by 20 mm FSP just below ballistic limit. Contours of the (a) pressure wave (b) sub-laminate interface failure in blue and (c) overlay of pressure wave front on bonded interface contour
5.2.2 Transition

In Chapter 3 it was found that thick UHMW-PE composite targets (typically > 10 mm) are perforated in two successive stages by 12.7 mm and 20 mm FSPs: an initial stage characterised by localised deformation and failure, followed by a back face bulging stage. This is also shown in the post-test target in Figure 5.12, where the back of the target forms a bulge and is detached from the front portion of the target. The transition between these two stages and the proportion of the thickness undergoing the initial stage was measured and reported as the shear plugging ratio in Chapter 3. This behaviour was captured in the numerical model, and the ratio of the target penetrated under the initial stage was recorded and plotted with respect to the impact velocity in Figure 5.16.
Figure 5.16: Ratio of target thickness penetrated in shear plugging ($t_S$) to the total target thickness ($t$), with respect to the impact velocity

For the numerical simulations, measurements were taken from one partial penetration and one complete perforation close to the ballistic limit for each target thickness and projectile combination. This is so a direct comparison can be made with the experimental results where the impact velocities were all very close to the $V_{50}$. Although there is a reasonable degree of scatter in both the experimental and numerical results, there is very good overall agreement between the two. Below impact velocities of around 500 m/s the UHMW-PE composite targets respond entirely through bulging. With increased impact velocity, the proportion of the target penetrated by shear plugging increases rapidly until it begins to plateau at about 75% of the total thickness above 1200 m/s. The transition behaviour and the resulting shear plugging ratio prediction is not artificially enforced in these models (i.e. by strategically placing sub-laminate along the location of expected transition). Instead a sufficiently high number of sub-laminates were used in the target (as detailed in the mesh refinement study) such that transition behaviour develops based on the underpinning mechanisms, independent of the sub-laminate thickness.

The high level of agreement with experiment allows further investigation of the transition phenomena using the numerical model. While Figure 5.16 provides insight of the transition phenomena, it is difficult to determine the mechanisms for transition from this because there are changes in both the target thickness and impact velocity (results are from ballistic limit test where impact velocity and target thickness are interrelated). In order to simplify the problem to gain a better understanding of the mechanisms at play, one of these parameters should remain fixed. The numerical model was used to investigate this effect by fixing the target thickness and varying the impact velocity. The shear plugging ratio was measured in this case for a 102 mm thick target impacted by 20 mm FSP, and the results plotted in Figure 5.17. With the target thickness fixed, the results show similar behaviour to Figure 5.16 where at low velocities the majority of the target thickness response is bulging. The shear plugging ratio increases with increased impact velocity and begins to plateau off at about 2000 m/s. Unlike Figure 5.16 however, there is a large range of impact velocity (between 500 m/s to 1750 m/s) where the
shear plugging ratio increases linearly.

Figure 5.17: Shear plugging ratio for 102 mm target impacted by 20 mm FSP

Figure 5.18 and Figure 5.19 show the pressure contours of a 102 mm thick target impacted by 20 mm FSP at low (1000 m/s) and high (2200 m/s) velocity. There are three images showing three important stages leading to transition to bulging. The first image is a contour of the initial pressure wave generated from impact. It shows that the pressure wave travels much faster ahead of the projectile in the case of low velocity impact compared to high velocity impact. The second image shows the point just after the pressure wave reaches the back of the target and the pressure is released, which also causes a velocity jump. A small localised bulge develops at the back as a result of the velocity jump and the release wave travels back towards the projectile, releasing the pressure in the material as it propagates. In the third image, the release wave reaches the projectile and the pressure in the target material ahead of the projectile is released. The material between the projectile and the back of the target undergoes bulging.
Figure 5.18: Stress wave propagation through 102 mm thick target impacted by 20 mm FSP at 1000 m/s

Figure 5.19: Stress wave propagation through 102 mm thick target impacted by 20 mm FSP at 2200 m/s
The influence of the shock, release and projectile position at the point of transition can be depicted in a position-time wave diagram as shown in Figure 5.20. The different transition points observed for the low and high velocity impact is due to the relative velocity of the pressure wave and the projectile. In the high velocity impact case, the projectile penetrates through more material by local penetration by the time the release wave reaches the projectile, so a greater portion of the target experiences local penetration. In contrast, the large difference between the projectile and shock velocity in the low velocity impact case means the release wave reaches the projectile much sooner; before the projectile can penetrate a significant amount of the target in local penetration. Figure 5.21 plots the shock velocity against the impact velocity of a 20 mm FSP on a 102 mm thick target. It shows that at low velocities, there is a large difference between the shock and impact velocity whereas at higher impact velocities the difference becomes progressively smaller.

Figure 5.20: Position-time wave diagram showing the influence of shock, release and projectile position on transition
The effect of impact velocity on the transition point for a constant target thickness is shown graphically in Figure 5.22(a). Figure 5.22(b) shows the influence of target thickness on the transition point for a constant impact velocity. The influence of impact velocity and target thickness on transition was already observed experimentally (in Chapter 3). This proposed mechanism for transition is consistent with these findings.

Figure 5.22: Position-time wave diagram showing difference in transition point due to (a) impact velocity and (b) target thickness
5.2.2.3 Bulge Development

The bulge deformation of UHMW-PE composite typically develops in a pyramidal shape for a cross-ply layup, as shown for example in Figure 5.23. Prediction of the bulge deformation is important as it is characteristic of the material wave properties and is an indication of the projectile position and velocity as it penetrates the target. Furthermore the bulge apex is an important parameter for many protection applications (i.e. back face deformation). The propagation behaviour of the hinge and apex in UHMW-PE composite strips and thin targets (up to four plies) is known (Chocron et al., 2013) and can be predicted using classical yarn impact theory (Smith et al., 1958) (discussed in the literature review). However, it is not currently possible to predict the propagation of the hinge and apex in thicker targets using analytical models or demonstrated using existing numerical modelling approaches. For thicker targets the position and velocity of the hinge is more difficult to predict because of the added complexities of progressive penetration process, a target where only a portion is undergoing bulging, and a projectile that is being significantly decelerated.

As shown in Figure 5.23, the deformation of the bulge is well replicated in the numerical model. The hinge and apex position of the bulge from 10 mm, 20 mm and 36 mm thick Dyneema® HB26 targets impacted by the 20 mm FSP just below the ballistic limit were measured in-situ using a high speed camera in the experimental program and reported in Chapter 3.

![Figure 5.23: Numerical (left) and experimental (right) target response at 450 μs after impact by 20 mm FSP at 888 m/s](image)

Figure 5.24 compares the hinge and apex positions with respect to time following projectile impact determined from experiment and numerical simulation. The development of the bulge apex is well predicted by the model for the 10 mm and 20 mm thick panels, but less accurate for the 36 mm thick panel. As shown in the previous section, the ballistic limit predictions were more accurate for the 10 mm and 20 mm thick targets against the 20 mm FSP (within 1% error in V_{50} prediction) while the V_{50} for the 36 mm target was over-predicted by up to 20%. The increased penetration resistance in the numerical model for the 36 mm thick target would be
identified in the bulge growth by an under-prediction of the apex position, as seen in Figure 5.24(e).

The hinge positions for the three cases investigated are in good agreement with experiment up to a hinge position of about 70 mm. For larger hinge displacement, numerical predictions deviate from the experiment, where the difference can be attributed to two different sources. Firstly for large hinge displacement, the hinge travel is affected by target clamping (which is not modelled) as it extends close to the target edge. If the target (which measures 300 mm \( \times \) 300 mm laterally) is impacted at the centre, the hinge can theoretically propagate up to 150 mm. However the presence of the vice clamps on the top and bottom (as shown in Figure 5.23) of the target reduces this to about 100 mm for this configuration. Off-centre impact and resistance to rotation at the clamp edge can reduce further the zone where the hinge can propagate unimpeded. Secondly, for large hinge displacement in the 20 mm and 36 mm target, the numerical model predicts a faster rate of hinge travel than experiment (as indicated by the slope of the curve). In section 3.3.2.3 of Chapter 3, the change in slope of the hinge position at large hinge displacement was attributed to the increase in resistance to material drawing as the transition plane between the sub-laminates grows. The numerical model however typically over-predicts delamination failure, so resistance to material drawing in to allow bulge growth is reduced. As such for large hinge displacement, the rate of hinge growth is higher in the numerical model than what is observed in experiment. Despite these issues, the numerical model still provides reasonably accurate predictions of bulge development, and is a capability not currently demonstrated by other modelling approaches.

Figure 5.24: Bulge apex and hinge position with respect to time for (a and b) 10 mm panel impacted at 365 m/s, (c and d) 20 mm panel impacted at 615 m/s and (e and f) 36 mm panel impacted at 888 m/s by a 20 mm FSP.
5.2.3 Ballistic Limit Over-Prediction

The proposed modelling methodology provides significant improvements to the current state-of-the-art not only in terms of qualitative and quantitative accuracy but also a considerably larger range of impact velocity for UHMW-PE composite. However the ballistic limit was shown to be consistently over-predicted for thick targets or high impact velocities cases, with differences typically between 10% to 20% of experiment. This section investigates possible reasons for this, focusing on two physical mechanisms not captured by the model: fibre bridging and the degradation in mechanical properties due to thermal softening.

5.2.3.1 Effect of Fibre Bridging

For numerical modelling of impacts just below the ballistic limit, one or more sub-laminates often delaminated fully from the target and were carried along with the projectile. Since the target was not perforated, this was considered a partial penetration result with a residual projectile velocity of 0 m/s. This was also observed in the ballistic experiments, although the extent of delamination is not as significant. In the experiment, fibre bridging between sub-laminates provide significant resistance to complete delamination and therefore detachment of a subsection of the laminate. In the numerical model, fibre bridging is not described because only a combined stress failure criterion is used to capture failure between the sub-laminate interface. Therefore the resistance of the sub-laminates to complete detachment is lower than if fibre bridging is included. This may be a reason for the over-prediction of the ballistic limit by the numerical model, particularly for thicker targets with increasing numbers of sub-laminate interfaces.

In order to determine the effect of fibre bridging on the numerical prediction, modification to the material parameters were made to replicate as best as possible fibre bridging effects. For this study, the 36 mm target impacted by 20 mm FSP was investigated. There are no studies on the effect of fibre bridging on the mode I or II fracture energy/toughness of UHMW-PE composite. However, studies on carbon fibre-reinforced composites show that fibre bridging increases the mode I fracture energy by up to four times (Pereira and de Morais, 2004). To simulate the effect of fibre bridging, the failure stress at the bonded interface was increased by four times. A comparison of the normal and shear failure stress at the bonded interface between the baseline and fibre bridging model is given in Table 5.8. Although not the same as fracture energy, increasing the failure stress at the bonded interface provides an indication of the effects of increased resistance to interlaminar fracture in the presence of fibre bridging.

<table>
<thead>
<tr>
<th>Table 5.8: Baseline and fibre bridging model</th>
</tr>
</thead>
<tbody>
<tr>
<td>Normal failure stress (MPa)</td>
</tr>
<tr>
<td>-------------------------------</td>
</tr>
<tr>
<td>5.35</td>
</tr>
<tr>
<td>Shear failure stress (MPa)</td>
</tr>
</tbody>
</table>

The ballistic impact results are compared in Figure 5.25. The effect of increasing the interlaminar strength (to simulate increased resistance to interlaminar failure due to fibre bridging) has negligible effect on the residual velocity and ballistic limit predictions compared to the baseline model. As such fibre bridging is ruled out as a reason for the over-prediction of the ballistic limit at large target thicknesses and high impact velocities.
Figure 5.25: Baseline and fibre bridging model ballistic residual velocity prediction for 36 mm target impacted by 20 mm FSP. The results are fit to the Lambert-Jonas equation, parameters for which are given in the legend ($a$, $p$ and $V_{BL}$).

Figure 5.26 compares the delamination contours for the baseline and fibre bridging model. The front, back and perspective view of the 36 mm target impacted by 20 mm FSP at 888 m/s (resulting in partial penetration) of the two models is compared. The contours show extensive delamination failure on the front and back face of the target in the baseline model compared to the fibre bridging model. This is expected as the fibre bridging model has a higher interlaminar failure stress and therefore a greater resistance to failure at the sub-laminate interface. In the model with increased interlaminar strength, the transition plane does not reach the edge of the target, so the rear portion of the panel remains attached to the target. Material pull in is still observed along the fibre direction orthogonal to the impact axis similar to the baseline model.
5.2.3.2 Effect of Thermal Softening

The over-prediction in ballistic limit of thick targets may be due to a lack of a thermal softening in the strength and failure model, which describes the degradation of the mechanical properties of the composite due to thermal loads. Thicker targets are impacted at higher velocities where the amplitude of the shock and the release in shock pressure permanently increases the temperature of the material. This process of temperature rise due to shock is depicted in Figure 5.27, which shows a material initially at point 0, shocked to point 1 on the shock Hugoniot along the Rayleigh line, followed by unloading along the release isentrope to point 2. The process is irreversible where the lost energy is indicated by the shaded region and the temperature of the shocked material increases from $T_0$ to $T_2$ (Meyers, 1994). The mechanical properties of UHMW-PE fibre are highly sensitive to thermal loads due to its very low softening (glass transition temperature of -160°C) and melting (~137°C) temperatures (Kurtz, 2004). Important properties such as the tensile strength and elastic modulus of UHMW-PE fibres, which are directly related to its ballistic performance, are significantly degraded under thermal loads (Dessain et al., 1992). There is extensive literature citing observations of fibre and matrix melting around the penetration cavity of UHMW-PE composite targets including this work and others (Taylor and Carr, 1999; Greenhalgh et al., 2013; Chocron et al., 2013). However, this melting is typically observed in the front portion of the target where the temperature rise occurs upon the release in pressure in the load bearing fibres due to fibre rupture from projectile penetration. Therefore the temperature rise occurs after the passage of the projectile and is inconsequential to ballistic performance. In the rear portion of the target however, the pressure is released and the temperature rises before the projectile reaches this part of the target. Evidence of this is
observed in experiment described in section 3.3.2.1 in Chapter 3 where fibres towards the back of thick target showed ductile modes of fibre failure. This is in contrast to high strain rate tension tests of UHMW-PE composite which show predominantly brittle fibre failure (Koh et al., 2008). Ductile modes of fibre failure for this material is promoted under high temperatures due to increase mobility of the molecular chains (Peijs et al., 1994).

In order to study the effects of thermal softening, the 36 mm target against the 20 mm FSP was modelled, where the material model for the bulging section of the target is modified with degraded material properties to simulate the effect of thermal softening. Only the bulging section of the target is modelled with degraded properties because material in this region experiences a release in pressure leading to a permanent temperature rise before the projectile has reached this part of the target.

![Figure 5.27: Temperature rise associated with shock waves (Meyers, 1994)](image)

In the numerical model, the Mie-Grüneisen EoS and the specific heat of UHMW-PE composite allows the temperature to be approximated; the procedure for this is documented in Century Dynamics (2005). Figure 5.28 shows the temperature contours from the baseline model of a 36 mm target impacted by 20 mm FSP at the point where bulging is initiated. Temperature measurements were taken from elements along the impact centre-line and an average temperature of 328°K (55°C) was measured. This temperature is below the melting temperature of UHMW-PE but sufficiently high to promote ductile modes of fibre failure (Dijkstra et al., 1989), consistent with experimental observations in section 3.3.2.1.
Figure 5.28: Temperature contours from baseline model of 36 mm target impacted by 20 mm FSP at 1020 m/s at 40 μs after impact.

Dessain et al. (1992) extensively investigated the effect of temperature on the mechanical properties of UHMW-PE fibres. The key results showing the effect on the fibre tensile strength, elastic modulus and failure strain are shown in the literature review in Figure 2.12. These results show that from 293°C to 328°C, there is a 30% reduction in the tensile strength, 24% reduction in the elastic modulus and negligible change in the strain-to-failure. A separate material data set was derived for UHMW-PE composite that takes into account the degradation of the in-plane tensile strength and elastic modulus due to the average temperature rise relative to the baseline model. The change in the relevant material properties is given in Table 5.9 and a comparison of the resulting in-plane tension stress-strain curve is shown in Figure 5.29, with all other properties unchanged in the thermally degraded material data set (the effect of other material properties on the ballistic performance is minimal as will be shown in a later parametric study).

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Symbol</th>
<th>Baseline</th>
<th>Thermal softening</th>
<th>Unit</th>
</tr>
</thead>
<tbody>
<tr>
<td>Young’s Modulus 22,33</td>
<td>$E_{22,33}$</td>
<td>$5.11 \times 10^7$</td>
<td>$3.88 \times 10^7$</td>
<td>kPa</td>
</tr>
<tr>
<td>Tensile Failure Stress 22,33</td>
<td>$S_{22,33}$</td>
<td>$1.15 \times 10^6$</td>
<td>$8.05 \times 10^5$</td>
<td>kPa</td>
</tr>
<tr>
<td>Plasticity constant 22,33</td>
<td>$A_{22,33}$</td>
<td>$6 \times 10^{-4}$</td>
<td>$1.4 \times 10^{-3}$</td>
<td>-</td>
</tr>
</tbody>
</table>
Figure 5.29: Baseline and thermally softened in-plane tensile stress strain curve due to temperature rise of 35°C from ambient.

The material data set describing degraded tensile properties due to temperature is applied to the bulging section of the target as shown in Figure 5.30. Experimentally this was found to be on average 35% of the thickness of the target (Figure 3.16 in Chapter 3), which corresponds to the last 6 sub-laminates out of 18 for this target. Although the temperature increase is localised to the region of material ahead of the projectile, the thermally degraded material data set is applied to the entire sub-laminates in the bulging section for simplicity of modelling. This is not expected to have significant influence on the results as the behaviour is dominated by local perforation.
Figure 5.30: Model accounting for thermal softening effects

The model accounting for thermal softening effects is simulated with increasing impact velocity and the residual velocity of the projectile is measured. Comparison of the residual velocity results using the baseline and thermally softened model is shown in Figure 5.31. As expected, with a drop in tensile properties, the residual velocity predictions using the thermally softened model are higher than the baseline model and leads to ballistic limit predictions closer to the experimental value.
Figure 5.31: Ballistic residual velocity prediction for 36 mm target impacted by 20 mm FSP using baseline and model incorporating effects of thermal softening. The results are fit to the Lambert-Jonas equation, parameters for which are given in the legend ($a$, $p$ and $V_{BL}$).

With ballistic limit predictions closer to the experimental value, the prediction of the hinge and apex position was re-evaluated for the 36 mm target, which was found to be under-predicted in the baseline model due to an over-prediction of the material penetration resistance. The results are given in Figure 5.32 and show improved predictions of the apex position compared to the baseline model. The hinge position is still in good agreement for small hinge displacements (<70 mm), but is still less accurate for large hinge displacements.

![Graph showing ballistic residual velocity prediction](image)

Further simulations employing the approach discussed above to account for thermal softening were performed for the other thicknesses to assess the ballistic limit predictions against the
A summary of the average temperature profile and corresponding change in the in-plane tensile properties for each thickness is shown in Table 5.10. Since the 10 mm and 20 mm targets experienced a similar average temperature rise, they were modelled using the same thermally degraded material data set. This was also the case for the 36 mm, 76 mm and 102 mm targets. The number of sub-laminates on the rear portion of the target with the applied thermally degraded material properties is also given in Table 5.10, reflecting the proportion of the target that experiences a release in the shock pressure before perforation (also the bulging section of the target), and therefore a temperature rise.

Table 5.10: Average temperature profile and corresponding in-plane tensile properties for various target thicknesses.

<table>
<thead>
<tr>
<th>Thickness (mm)</th>
<th>Average Temperature Rise (°C)</th>
<th>Simulated Temperature Rise (°C)</th>
<th>Young’s Modulus 22,33 (E22,33) (GPa)</th>
<th>Failure Strength 22,33 (S22,33) (MPa)</th>
<th>Plasticity constant 22,33 (A22,33)</th>
<th>Sub-laminates modelled with thermally-degraded properties</th>
</tr>
</thead>
<tbody>
<tr>
<td>Baseline</td>
<td>51.1</td>
<td>51.1</td>
<td>1150</td>
<td>975</td>
<td>9.1 x 10^-4</td>
<td>5/1</td>
</tr>
<tr>
<td>10</td>
<td>15</td>
<td>18</td>
<td>45.5</td>
<td>975</td>
<td>9.1 x 10^-4</td>
<td>5/5</td>
</tr>
<tr>
<td>20</td>
<td>21</td>
<td>18</td>
<td>45.5</td>
<td>975</td>
<td>9.1 x 10^-4</td>
<td>5/10</td>
</tr>
<tr>
<td>36</td>
<td>35</td>
<td>35</td>
<td>38.8</td>
<td>605</td>
<td>1.1 x 10^-3</td>
<td>6/18</td>
</tr>
<tr>
<td>76</td>
<td>38</td>
<td>35</td>
<td>38.8</td>
<td>605</td>
<td>1.1 x 10^-3</td>
<td>9/38</td>
</tr>
<tr>
<td>102</td>
<td>35</td>
<td>35</td>
<td>38.8</td>
<td>605</td>
<td>1.1 x 10^-3</td>
<td>13/51</td>
</tr>
</tbody>
</table>

The impact-residual velocity and ballistic limit results are shown in Figure 5.33. Compared to the baseline model, there is a significant improvement in the predicted ballistic limit velocity for the thicker targets impacted at higher velocities when thermal softening is taken into account. The ballistic limit predictions are now within 5% of the experimental value. For thinner targets impacted at lower velocities, the results are similar to the baseline because the temperature rise due to shock induced heating is small and therefore has little effect on the ballistic performance of the target. These results therefore show that the omission of thermal softening in the strength model is a major reason for ballistic limit over-prediction of thicker targets in this study.

![Figure 5.33](image)

**Figure 5.33:** (a) Residual-impact velocity results fit to the Lambert-Jonas equation, parameters are given in the legend ($a$, $p$ and $V_{BL}$) and (b) ballistic limit results for targets impacted by 20 mm FSP

### 5.3 Parametric Study

A parametric study is performed using the baseline data set in Table 5.1 to provide further insights into the influence of material properties on the ballistic performance. For instance,
changes to certain properties can change the penetration and damage modes exhibited by the
material, which can lead to an improvement or reduction in ballistic performance. As such
understanding the influence of these properties is critical to the development of the material
and armour designs that take advantage of certain mechanisms.

In order to assess the sensitivity of the model to changes in the mechanical properties, finite
and semi-infinite targets are modelled so that any difference in penetration mechanisms can be
assessed. For the finite thickness target a 20 mm target impacted by 20 mm FSP with a large
over-match velocity of 904.3 m/s was modelled. A large over-matched condition is modelled
because there is significantly less scatter in the residual velocity compared to near the ballistic
limit. For this condition, experimental measurements recorded a residual velocity of 739±35
m/s, while the baseline model predicted a residual velocity of 658 m/s. The model sensitivity
for the semi-infinite cases was assessed on the depth of penetration of a 150 mm thick target
impacted by a 20 mm FSP at 1304.6 m/s. Experiments reported a depth of penetration of 61.4
mm while the baseline model predicted 60 mm of penetration for this case.

For both of these models, the mechanical properties of interest were increased to 150% of the
baseline value while keeping all other values constant. The properties investigated in this study
include the in- and out-of-plane elastic modulus, shear modulus, failure strength, fracture energy,
failure strength at the sub-laminate interface, and the damage coupling coefficient. Since the
material is orthotropic (cross-ply layup), properties in the in-plane 22 and 33 direction were set
to the same value. The shear 12 and 31 directions were also treated in the same manner. For
the in-plane tension properties, the in-plane tensile strength $S_{22}$ and $S_{33}$ and the out-of-plane
shear strength $S_{12}$ and $S_{31}$ were all increased by 150%. This is because the out-of-plane shear
was determined as a function of the in-plane tensile strength in the baseline model (maximum
principal shear stress from in-plane tensile strength ($S_{ij} = S_{ii}/2$)). Since the failure criterion
at the sub-laminate interface is based on a combined normal and shear stress component, the
failure strength for each of these components was increased by 150%, to allow the sensitivity
of the model to each failure component to be studied. Both normal and shear failure strength
components were also increased to the quasi-static through-thickness tensile strength of aramid
and carbon epoxy composites (50 MPa), and the dynamic spall strength of aramid epoxy (100
MPa) and carbon epoxy (250 MPa) composites (Riedel et al., 2003b).

The effect of increasing the mechanical properties on the projectile residual velocity and depth
of penetration is determined by normalising the results with respect to the baseline results as
follows:

$$
1 - \frac{V_R}{V_{R,ref}} \quad \text{and} \quad 1 - \frac{DoP}{DoP_{ref}}
$$

(5.28)

where $V_R$ is the projectile residual velocity, $DoP$ is the depth of penetration, and subscript
$ref$ refers to the reference results from the baseline model. Table 5.11 shows the results of the
parametric study for both the over-matched and semi-infinite depth of penetration target. Here,
results less than zero correspond to lower projectile residual velocity or depth of penetration
relative to the baseline model and indicate increased resistance to penetration and ballistic
performance.
Comparing the percentage difference in the residual velocity and depth of penetration, the difference in the residual velocity relative to the baseline model is typically larger than the difference in the depth of penetration. This is because the residual velocity is non-linearly related to the impact velocity (i.e. Lambert-Jonas curve) whereas DoP is linearly related to the impact velocity. As such, benchmark values based on residual velocity are typically more sensitive to parametric changes than DoP measurements. Most of the results are within 5% of the baseline value which is typically the scatter range of the numerical predictions.

Several key observations can be made from the results:

- The increase in the elastic and shear modulus leads to a small reduction in ballistic performance. Although it is favourable to have a high in-plane modulus to increase the wave speed of material and therefore the rate at which the load is distributed (in fabrics and composites), an increase in the modulus also reduces the strain energy accumulated before failure (from the area under the stress-strain curve). This result suggests the strain energy absorbed before failure is more important to ballistic performance than the increase in wave speed.

- For finite targets an increase in the in-plane shear modulus leads to a reduction in the penetration resistance compared to a negligible change in the depth of penetration. This property is important for finite thickness targets because large in-plane shear deformation occurs under bulging. An increase in the stiffness leads to a reduction in the bulge deformation and therefore a reduction in the penetration resistance. There is no bulge deformation in the semi-infinite depth of penetration case so the impact of an increased in-plane shear modulus on penetration resistance is negligible.

- Increase in the coupling between damage modes reduces the ballistic performance. This result is expected as coupling of failure mode reduces the material overall ability to carry loads. The effect is small (only about a 10% reduction), compared to the overall change in the coupling coefficient.

- An increase in the interlaminar shear and tension strengths at the sub-laminate interface leads to a small reduction in the ballistic performance. Higher interlaminar strength
reduces the targets susceptibility to interlaminar failure. Impact energy therefore can not be absorbed by breaking the bonds at the sub-laminate interface, reducing the ballistic performance. Furthermore, bulging is inhibited as the rear portion of the target can not break away from the target due to high interlaminar strength. The overall influence is however very small, approximately 5%, compared to an increase in the interlaminar strength by up to 50 times in the most extreme case.

- Increasing the strength of the material (specifically the in-plane tensile strength together with the out-of-plane shear strength) leads to the biggest improvement in the ballistic performance. The importance of the tensile strength on the ballistic performance has been shown experimentally and extensively discussed throughout the literature review. Furthermore the influence of the tensile strength on the critical velocity of fibre, yarns and thin strips on the ballistic performance of thin and thick targets have been demonstrated using classical yarn impact theory, membrane-based analytical models and the new analytical model for thick composites in Chapter 4.

This parametric study shows the in-plane tensile strength of UHMW-PE composite has the most influence on the ballistic performance. This finding is consistent with existing experimental and analytical observations. In comparison, only minor effects on the ballistic performance (less than 10%) are observed for a 150% or more increase to every other material parameter. The increase in fibre tensile strength is therefore critical to improving the ballistic performance of the composite armour. For this reason the historical development of UHMW-PE fibre has focused on improving the tensile strength of the fibres, and has shown a steady increase using more advanced gel spinning techniques. From a numerical modelling perspective, accurate characterisation of the in-plane tensile properties is also critical for accurate predictions of ballistic performance.

5.4 Conclusion

In this chapter a numerical modelling methodology is developed to analyse thick UHMW-PE composite panels under ballistic impact using the non-linear orthotropic model in a commercial hydrocode (ANSYS® AUTODYN®). The approach overcomes existing weakness in the non-linear orthotropic model that were identified in the evaluation study in the literature review chapter, and provides accurate prediction of thick UHMW-PE composite targets impacted below 2000 m/s. A sub-laminate discretisation of the target was introduced to overcome the inherent issues in the failure model that lead to premature through-thickness failure in the existing model. This approach is also more accurate in replicating interlaminar failure in the target. A new damage-based erosion model was written in a user subroutine which is more suitable for anisotropic materials because of their directional dependent strength and failure compared to existing strain-based models. A new material data set is derived for UHMW-PE composite using experimental test data from literature.

The methodology was extensively validated against experimental ballistic impact data. Depth of penetration into semi-infinite targets against 20 mm FSPs was predicted to within 3% of experimental values. Ballistic limit and residual velocity predictions were also in good agreement with experimental measurements, with the ballistic limit predictions within 20% for all conditions considered (12.7 mm and 20 mm FSP, impact velocity between 400 m/s to 2000 m/s and target thickness between 10 mm to 100 mm). The over-prediction was shown to be due to the omission of thermal softening in the strength model. An approximate approach was
incorporated to account for thermal softening, which improved the ballistic limit predictions to within 5% of the experimental value.

The two stages of penetration exhibited by thick UHMW-PE composite targets was also well replicated with the model, which gives good predictions of the shear plugging thickness ratio. The bulge development of UHMW-PE composite under ballistic impact is accurately modelled in terms of the bulge hinge and apex position, which has not been demonstrated before using existing modelling approaches.

The numerical model was used to gain further insights into the transition and delamination behaviour of UHMW-PE composite under impact, which were difficult to determine experimentally. This study found transition to bulging occurred at the point where the released wave from the initial (shock) wave reaches the projectile, causing a velocity jump and bulge deformation of the material ahead of the projectile. Delamination was shown to occur under high pressure and follows the contours of the pressure wave front. Through this process, delamination planes develop ahead of the projectile during penetration, reducing the bending resistance of the panel ahead of the projectile and allowing large bulge deformation to occur.

The parametric study provided improved insights into the influence of material properties on ballistic performance of UHMW-PE composite. The study showed the in-plane tensile strength has the biggest influence on performance while an increase in all other properties has minor (typically adverse) influence on the ballistic performance. The study also showed that an increase in the elastic and shear modulus results in a small reduction in performance because the strain energy is reduced, while the in-plane shear properties is shown to be only important for finite thickness targets where bulging occurred.
Chapter 6

Conclusion

In this PhD project, the key penetration and failure mechanisms exhibited by thick UHMW-PE composite were experimentally characterised. Post-test analysis of the targets impacted by FSPs showed penetration of thick targets occurs in two stages; an initial shear plugging stage followed by a bulging stage where the rear portion of the target breaks away and exhibits large global deformation. Thin targets were found to exhibit only the later bulging stage. Scanning electron microscopy of the fibres in the penetration cavity showed the dominant failure mode in the shear plugging stage was fibre shearing, while fibre tension failure was dominant in the bulging stage. The extensive experiments carried out in this work allowed the proportion of the target under shear plugging and bulging at close to the ballistic limit to be thoroughly characterised for the first time. This investigation found the proportion of the target under shear plugging is related to, and increases with, increased target thickness and impact velocity. Furthermore, this behaviour was found to plateau for target thickness above 35 mm and impact velocities greater than 1200 m/s to about 75% of the laminate thickness.

The ballistic limit velocity ($V_{50}$) of UHMW-PE composite was determined for targets up to 100 mm thick against 12.7 mm and 20 mm FSPs, which found the performance increases linearly with increased thickness. Furthermore, it was shown that ballistic limit results for different calibre FSPs were directly scalable using the non-dimensional areal density. The ballistic limit results were used to evaluate the mass and space efficiency of UHMW-PE composite compared to other common metallic and fibre-reinforced composite armour materials against FSPs. This investigation showed UHMW-PE composite has the highest mass efficiency of all materials considered (RHA steel, HHA steel, aluminium alloy, CFRP, AFRP and GFRP): between 300% to 500% compared to armour steel and aluminium, and 130% to 160% compared to the fibre-reinforced composites. UHMW-PE composite space efficiency is however typically lower than armour steel, AFRP and GFRP. Furthermore, thinner UHMW-PE composite targets were found to be relatively more efficient than thicker targets. This is because a greater proportion of the target undergoes penetration by membrane tension.

A new analytical model was developed to describe the penetration process of thick UHMW-PE composite targets and provide an approximation of ballistic performance against blunt projectiles. The analytical model was derived using the knowledge developed from the experimental work. The model was described in two components: a shear plugging component described using conservation of energy, and a bulging component described using momentum conservation and classical yarn impact theory. These were combined with the energy balance between the energy absorbed by the target and the kinetic energy of the projectile, to derive an expression for the ballistic limit velocity. The model was validated against the current experimental results for the
12.7 mm and 20 mm FSP as well as 5.56 mm FSP data from literature. Excellent agreement with experimental results is seen for thick targets exhibiting two stages of penetration. For thin targets exhibiting only bulging, existing membrane models were shown to be more applicable.

A numerical modelling methodology was developed for the analysis of thick UHMW-PE composite under ballistic impact. An existing orthotropic continuum model which captures non-linear shock compression, orthotropic elastic-plastic strength, and orthotropic failure was evaluated against thick targets for impact velocities below 2000 m/s. This study highlighted several weaknesses in the model, including coupling in the out-of-plane direction of the failure model that leads to premature element failure. The strain-based erosion model was shown to be inappropriate for composites because it does not account for the properties of the composite in different directions. These deficiencies resulted in very poor ballistic limit velocity predictions. These problems were addressed in this work in order to capture the ballistic impact response of UHMW-PE composite. A sub-laminate discretisation method was developed to overcome the deficiencies in the out-of-plane failure model, which allows the through-thickness tensile and shear failure to be decoupled in the bulk material and delamination to be better modelled. A new failure-based erosion model more suitable for anisotropic materials was incorporated using a user subroutine. A new data set was also derived for Dyneema® HB26 using experiments reported in literature, which contain more accurate characterisation methodologies for UHMW-PE composite. These changes to the model were implemented in the existing material model and was extensively validated against experimental depth of penetration and ballistic limit results conducted in this project. Excellent agreement with experiment was seen for depth of penetration on semi-infinite targets against 20 mm FSPs and ballistic limit and projectile residual velocity against 12.7 mm and 20 mm FSPs on targets up to 20 mm thick. For thicker targets up to 100 mm thick that were subjected very high impact velocities and large amounts of shock heating, the model typically over-predicted the ballistic limit velocity by up to 20%. The shear plugging ratio and the bulge growth behaviour, in terms of the apex and hinge position, was also accurately captured although delamination area was over-predicted. Despite this, the level of accuracy for such a wide range of conditions (12.7 mm FSP and 20 mm FSP, 10 mm to 100 thick targets and impact velocity between 400 m/s to 2000 m/s) has not been demonstrated before and is a significant improvement over existing continuum modelling approaches.

The numerical model was used to further investigate the ballistic impact behaviour of UHMW-PE that were difficult to characterise experimentally. Transition from shear plugging to bulging mode of penetration was shown to be dependent on the impact velocity, target thickness, rate of projectile penetration into the target, and the shock and release wave characteristics of UHMW-PE composite. The position of transition was shown to be dependent on the position of the projectile at the time the release wave reaches the projectile, causing all of the material ahead of the projectile to bulge. Delamination was shown to be predominantly driven by the initial shock wave that leads to delamination in material ahead of the projectile. This reduces the bending resistance of the target and allows the target to exhibit large deformation when bulging is initiated. Although the modelling approach does not capture fibre bridging and thermal softening effects, parameters were modified to simulate the effect of fibre bridging and thermal softening on the ballistic performance. Fibre bridging was found to have negligible impact on performance while the effect of thermal softening can be significant where impact velocities are high enough to cause significant shock-induced thermal loads. An approach was incorporated to account for thermal softening, which improved the ballistic limit prediction of thicker targets impacted at higher velocities to within 5% of the experiment.
6.1 Recommendations

Although significant progress has been made in understanding and modelling the response of thick UHMW-PE composite to ballistic impact, areas for future work have also been identified throughout the course of this thesis to extend the state-of-the-art.

Despite the success of the numerical model in predicting the ballistic performance of UHMW-PE composite for the wide range of conditions investigated, several aspects of the model can still be improved. The numerical model proposed a sub-laminate discretisation of the laminate in order to better model delamination failure. For this to occur, the sub-laminates are joined together using bonded contacts where failure is initiated based on a combined normal and shear stress criterion. Such approach is known to be mesh-dependent and has been replaced by fracture energy based failure (mode I, II and III) (Mi et al., 1998). Most finite element models of fibre-reinforced composite today capture interlaminar failure through zero thickness cohesive elements which accounts for both damage and fracture mechanics (using traction-separation laws), though this is not available in the hydrocode used in this work. For UHMW-PE composite, an energy-based approach to modelling the failure of the sub-laminate interface can further improve the accuracy. This is important as the current approach typically over-predicts the delamination area. Although this study showed the over-prediction in delamination is not critical for performance, accurate representation of the integrity of the target after impact is critical if the model is to be considered for simulating multi-hit scenarios.

The inability of the model to capture material softening and failure due to thermal loads was shown to be a major reason for the over-prediction of ballistic performance for thicker targets. Inclusion of thermal softening effects on the material strength and failure is therefore critical to increasing the accuracy of the model for a wider range of problems involving thicker targets impacted at very high velocities. In this work, an approach was developed to simulate the effect of thermal softening. This approach required an initial simulation with the baseline model to find the temperature profile that results from shock induced heating, followed by a second simulation that accounted for the temperature rise through manual adjustment of the material parameters. These simulations showed the effect of thermal softening on the ballistic performance, although it is recommended that the effect of thermal softening should be directly incorporated into the strength and failure model. However for such a complex material model with already so many parameters, the inclusion of thermal effects can significantly increase the complexity of the strength and failure model. This can render the material model unusable due to its own complexity and need for extensive material characterisation. Instead, such implementations should consider thermal degradation only in the in-plane fibre direction, as the model was found to be most sensitive to this parameter in the parametric study.

In both the analytical and numerical models, the through-thickness shear strength was approximated using maximum principal shear stress theory in absence of experimental through-thickness shear data for UHMW-PE composite. This was justified by the fact that Kevlar® epoxy composite (a similar material) shows through-thickness shear strengths that are consistent with the maximum principal shear stress theory. However, future work should strive to experimentally characterise this important property and verify its consistency with principal stress theory. While an attempt has been reported in literature to characterise the through-thickness shear properties of UHMW-PE composite, loading under quasi-static rates caused fibre realignment and failure strength was not measured. It is believed that such test for UHMW-PE composite must be carried out at high strain rates in order avoid fibre realignment and induce pure shear failure.
Generally, the field of ballistic and penetration mechanics is extensive due to the unlimited combination of targets and threats. The response of targets is different depending on the projectile size, geometry, material and impact velocity. The scope of this thesis was restricted to fragmentation threats (where FSPs are used as a representative surrogate), however understanding the penetration and failure mechanisms of the material impacted by different projectiles (spherical and ogive) can also be valuable. This work also only considered normal impacts (the worst case scenario), however attacks experienced on the front line are almost always at an oblique angle (large or small). Understanding how UHMW-PE composite responds to obliquity is important and deserves attention. Extension of the analytical model to different projectiles can be made, although more validation of the numerical model against different threats and impact obliquity would be more practical. It is important to understand the applicability and limitations of the model, so that the use of such tools in the future is well informed.
References


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Appendix A

Erosion Model Subroutine

!******************************************************************************
SUBROUTINE SOLVEERO_USER_1_3D (ISTAT)
!******************************************************************************

USE material
USE ero_user_1
USE mdgrid3
USE wrapup
USE mdvar_all ! TO ACCESS ALL NODE AND ELEMENT VARIABLES
USE mdstrng
USE mdsolv
USE cycvar

IMPLICIT NONE

! DEFINE VARIABLE
INTEGER (INT4) :: ISTAT
REAL (REAL8) :: EPS_UNSTR, DMG22, DMG33, !Material parameter
                IGS_CUTOFF, DMG22_CUTOFF, DMG33_CUTOFF !Erosion cutoff

! DEFINE CUTOFF VALUES FOR EROSION
IGS_CUTOFF=1.3 !Element erosion at 150% effective strain
DMG22_CUTOFF=1 !Element erosion at in-plane damage
DMG33_CUTOFF=1 !Element erosion at in-plane damage

CALL GET_ELEM_VAR(ELEM_NOW,0)

! CRITERION 1: DAMAGE 22
DMG22=RLV(IVR_DAM22)
IF (DMG22==DMG22_CUTOFF) THEN
    ISTAT=1
END IF

! CRITERION 2: DAMAGE 33
DMG33=RLV(IVR_DAM33)
IF (DMG33==DMG33_CUTOFF) THEN
    ISTAT=1
END IF

! CRITERION 3: EFFECTIVE STRAIN
EPS_UNSTR=RLV(IVR_EFS)
IF (EPS_UNSTR==IGS_CUTOFF) THEN
    ISTAT=1
END IF

RETURN

END SUBROUTINE SOLVEERO_USER_1_3D