A Thermally Dependent Hydrocode Model for Ultra High Molecular Weight Polyethylene Under Hypervelocity Impact

Shaun G. Austin*

University of New South Wales at the Australian Defence Force Academy

The commercial hydrocode ANSYS Autodyne® was used to create a thermally dependent model of the Ultra High Molecular Weight Polyethylene (UHMW-PE) composite Dyneema® HB26 under hypervelocity impact. This composite is increasingly being used in armour applications at the ballistic impact range, and shows potential for application in aerospace protection systems. Due to the high monetary costs of hypervelocity impact experiments it is essential to have an accurate and time effective computational model to conduct preliminary testing of new armour designs. Many models have been created to simulate impacts with UHMW-PE composites like Dyneema® in their Uni-Directional (UD) ply form, however, none of these models automatically incorporate the effects of shock melting or thermal softening. In hypervelocity impact conditions, the shock waves propagating in the Dyneema® composite can be sufficient to soften the material and cause melting and de-crystallisation of the constituents. In the broad spectrum of impact velocities this composite may be applied it is necessary to incorporate these effects in order to establish a reliable predictive capability. To introduce this thermal dependence, an existing model was validated then enhanced through the use of a custom user subroutine to update the material parameters relative to the temperature of discrete sub-laminate layers.

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*LT, School of Engineering & Information Technology, ZEIT4500

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Nomenclature

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<tr>
<td>$C_0$</td>
<td>Material Speed of Sound</td>
<td>[mm/µs]</td>
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<td>$C_f$</td>
<td>Fibre Speed of Sound</td>
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<td>$\rho_0$</td>
<td>Initial Density</td>
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<tr>
<td>$\rho$</td>
<td>Density at the Shock Front</td>
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<td>Specific Volume at the Shock Front</td>
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<td>$\sigma$</td>
<td>Effective Stress</td>
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<td>$L_{ii}$</td>
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I. Introduction

Ultra High Molecular Weight Polyethylene (UHMW-PE) is a form of polyethylene that has been gel-spun and consolidated into fibres with high molecular chain orientation and crystallinity (up to 95% in such commercial fibres as Dyneema®). Composites made from these fibres are widely used in ballistic protection applications due to their low weight and high penetration resistance. Due to their high elastic modulus in the fibre direction and low density, UHMW-PE fibres have a high speed of sound which “disperses the strain wave rapidly away from the impact point”, thus “distributing the energy over a wider area.” The combination of these properties makes UHMW-PE composites viable for use in ground based armour and aerospace applications. To prove the validity of these composites for such applications, they must however be tested under all expected impact conditions.

A. Motivation

This project was initiated from the opportunity to model the commissioning tests of the University of New South Wales (UNSW) Canberra Two-Stage Light Gas Gun (TSLGG). In the process of investigating this problem it was found that no computational model existed in the open literature that was able to incorporate the thermal softening effects of UHMW-PE into a comprehensive material model for the accurate prediction of the material behaviour under impact. Such models are essential for the cost effective validation of new armours in their design phase. By creating an accurate computational model based on past experimental data, external agencies or researchers wishing to use the TSLGG can validate an UHMW-PE armour design before going through the rigours and expenses of physical testing.

B. Objectives

The key objectives of this work were as follows: to produce an easy to use hydrocode model for researchers and external agencies that have moderate knowledge of ANSYS Autodyn®, to accurately model hypervelocity impacts against UHMW-PE composites of Uni-Directional (UD) ply Dyneema®, and to improve the accuracy of existing models through the incorporation of thermal softening effects due to shock heating and melting.

C. Methodology

This project was approached progressively in order to achieve the aforementioned objectives. Firstly, using existing hydrocode models of UD ply Dyneema® HB26, a model was developed and validated against material data and physical experiments. Following this, a user defined subroutine was written to incorporate the effects of shock heating into the material data. This subroutine was then tested iteratively until an appropriate level of thermal softening was accounted for to accurately predict the deformation, damage and residual velocity of a range of target thicknesses and impact velocities.
II. Impact Dynamics of UHMW-PE

Through an investigation into the literature on UHMW-PE composites under impact, the following categories were found critical to understanding the material and its behaviour in order to produce an accurate predictive model: Laminate Microstructure, Penetration Mechanics, Stress Waves and Thermal Effects. A summary of each category is provided below.

A. Laminate Microstructure

UHMW-PE composites comprise an arrangement of UHMW-PE fibres either in orthogonally layered UD plies or in woven fabric layers held together by a matrix material. These UHMW-PE polymers can contain upwards of 250,000 monomer units. For UD ply pre-impregnated lamina, the fibres are coated with a polyurethane (PU) resin as the matrix material and hot-pressed into an orthogonal laminate ($0/90^\circ$). For this work, the lamina chosen are Dyneema® HB26 which consist of approximately 86% UHMW-PE (by weight) and 14% PU resin.

B. Penetration Mechanics

The means by which a Fragment Simulating Projectile (FSP) penetrates an UHMW-PE composite is largely a function of the laminate microstructure, the thickness of the composite and the speed and shape of the FSP. Further effects on penetration come as a result of these factors, for example, the speed of sound in the material and thus the way in which waves propagate are a function of the laminate microstructure, as are the effects of shock heating due to these stress waves. For UHMW-PE composites, Sockalingam et al. explain “the projectile geometry not only affects the ballistic limit, but also plays a major role on the energy absorption behaviour and failure mechanisms”. Hazell states there are five main penetration mechanisms for composites, however, those particularly relevant are shear plug failure and lamination failure. Shear plug failure is the shear failure of the target below the point of impact creating a separate piece of material or ‘plug’ and is one of the common forms of deformation and failure. Lamination failure can be through sequential delamination, fibre pull-out or fibre tensile failure caused by back-face bulging. In UHMW-PE composites of UD ply, energy absorption generally occurs in two stages; firstly, by localised deformation and failure, and secondly, by back-face bulging (usually of a pyramidal form). This is dependent, however, on the velocity of the FSP, as Nguyen et al. explained.

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 1200 m/s and above.

The shape of the projectile also affects the degree of shear plugging to back face bulging. A similar effect is seen with woven fabrics as “higher velocities and sharper projectiles tend to fail fabrics and compliant laminates by shearing across the yarns, rather than extending them to failure”. Although ply cracking is another means of energy absorption and structural degradation, it is the delamination at inter-ply interfaces that dominates the response of UD plies under impact. Flat headed or cylindrical FSP’s such as those used in this work are found to cause greater shear failure in the initial fabric layers due to their sharp edges, whilst causing tensile failure in the rear layers.

C. Stress Waves

Under hypervelocity impact conditions at the moment of collision, the dynamic pressure immediately induces tensile fracture of the plies at the point of impact and simultaneously sends stress waves throughout the material. Hazell et al. defines the stress waves to travel in two forms; one longitudinally down the fibres at the speed of sound of the material, and the other, slightly slower, propagating through the matrix material. As UD ply laminates are orthotropic the elastic wave speed is calculated using Eq. (1).

$$C_f = \sqrt{\frac{E_f}{2\rho_f}}$$  \hspace{1cm} (1)
Here the factor of a half is to account for the material cross-ply lay-up. Sockalingam et al. demonstrated that unlike the longitudinal wave speed, the transverse wave speed is dependent on both the longitudinal wave speed and the impact velocity. As the transverse (compressive) stress wave travels through the material layers, the majority of the damage to the top layers is in the form of through-thickness shearing. Through the longitudinal fibres, the tensile wave dissipates the stress throughout the material. Hazell explains that “A high elastic wave velocity is important as this allows for the rapid delocalisation of the stresses from the point of impact.” This property directly influences the fibres specific energy absorption as this is a measure of the fibres ability to absorb the projectiles kinetic energy on impact. Following impact the transverse wave reaches the rear face of the target and is reflected back as a tensile release wave, which in turn causes the delamination ahead of the projectile by exceeding the through-material tensile strength. Similar to how the longitudinal waves help dissipate the impact energy; these transverse waves accelerate the target in the direction of travel of the FSP, thus decreasing the pressure between the FSP and the target. This acceleration caused by the work done to penetrate the first plies then allows for the laminate to transition to membrane tension and back-face bulging. The above explanation of stress wave propagation covers plastic and elastic stress waves; however, in hypervelocity impact shock waves are also generated. The shock wave velocity is related to the particle velocity using the non-linear shock Hugoniot equation in Eq. (2).

\[ U_S = C_0 + S_1 \mu_p + S_2 \mu_p^2 \]  

(2)

For Dyneema® UD ply composites, \( S_1 \) and \( S_2 \) are constants of values 3.45 and -0.99 \( \mu s/mm \) respectively and \( C_0 \) is the bulk sound speed of 1.77 mm/\( \mu s \). The density of the shock front is then a function of both \( U_S \) and \( \mu_p \) and is described by Eq. (3).

\[ \rho_0 U_S = \rho(U_S - \mu_p) \]  

(3)

This relationship allows the density and hence specific volume to be linked to the temperature induced by the shock wave as will be explained in the following section.

D. Thermal Effects

There are three main forms of heating caused during impact: friction, plastic work and shock waves. Prevorsek et al. demonstrated that at ballistic velocity ranges, heat generation was due to friction and that it had no effect on the ballistic performance of the UHMW-PE composites tested. The volume of material affected by the temperature rise was insignificant (approximately 1 mm\(^3\)) as the thermal conductance of the material was far too low to dissipate the heat in the time of the impact event. The minimal effect frictional heating has on impact experiments may be a key reason it has been widely overlooked in past literature and hydrocode models. The other modes of heating, shock wave and plastic work do however have more significant effects on the behaviour of UHMW-PE composites under impact. Shock heating can be explained in terms of the shock Hugoniot in Eq. (2), as the pressure of the shock wave passes a point in the material, that point is instantly moved from one location on the Hugoniot curve to another with an increase in entropy. Following the shock wave, rarefaction waves return the material along an isentropic curve close to its initial condition with the increase in entropy and hence heating caused by the shock wave. As UHMW-PE composites such as Dyneema® and Spectra® have melting temperatures in the order of 144°C to 152°C it is possible to exceed this temperature through the heat generated by shock waves during hypervelocity impact. Hazell et al. demonstrated “the temperature rise during shock loading can be approximated by examining the temperature along the adiabat (\( T_a \))” according to the Eq. (4).

\[ T_a = T_1 \exp[\Gamma - \Gamma \frac{U}{v_0}] \]  

(4)

Cheeseman et al. explained that effects of shock heating include “fibre fusion, bridging and contraction, along with crystallinity changes in the UHMW-PE filaments.” The effects of shock melting can be seen in works by Hazell et al. in which samples of Dyneema® were tested using plate-impact experiments. Although accounting for a small degree of error in most ballistic range experiments, thermal loads do arise and can cause fibre and matrix melting around the penetration cavity. In higher velocity experiments conducted with thick UHMW-PE targets, greater levels of melting are observed, corresponding to larger error as Nguyen et al. stated.
the ballistic limit prediction 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% (most likely due to the omission of thermal softening).

Through the literature analysed in this work, the only methods to model such effects have been in terms of frictional heating by Prevorsek et al. as previously explained and in work by Long Nguyen. The latter involved applying manually softened material parameters to the heated sub-laminate layers based upon an approximation of the material properties at the elevated temperature. This approximation can be made by following the near linear trend for the tensile stress limit and Young’s Modulus of Dyneema between room and melting temperature. This trend was demonstrated by Dessain et al. and is presented in Fig. 1.

Figure 1. Temperature dependence of Dyneema® SK60 fibre for tensile strength, strain to failure and Young’s modulus courtesy (Dessain et al. 1992).

The above method of approximation is used and discussed further in this work as the basis for the temperature dependence of the custom subroutines.

III. Target Model

MODELLING UHMW-PE composites under impact using computational hydrocodes has largely been conducted using Lagrangian codes. These codes use fixed elements of mass defined by a mesh and the finite difference method to calculate the material volumetric response, wave propagation, stress, strain and failure. The methods of using Lagrangian models can be divided into three main categories: constituent level where each fibre is modelled, meso-mechanical level in which the lamina are represented by sub-laminate layers of the composite geometry, and macro-mechanical level where the entire laminate is represented by a single homogenous block. A meso-mechanical model was chosen due to the ability to approximate delamination as a form of failure and will be discussed in the following sections.

A. Geometry

The standard model geometry was replicated from previous works with targets sized 300 mm x 300 mm at thicknesses of 10 mm, 20 mm and 36 mm. A mesh convergence study by Nguyen et al. found the largest sub-laminate thickness that complied with a 95% correlation to depth of penetration tests with semi-infinite targets. Using these results the targets were separated into 2 mm thick layers. The mesh applied utilised a centre bias to ensure the elements in vicinity of the point of impact were cubic whilst decreasing the overall computational complexity and hence run-time of the simulations. A 0.1 mm gap was placed between each layer for the use of an external gap contact algorithm. It was established in previous works that gapless contacts “resulted in over-penetration of elements leading to poorer predictions.” So long as the material was modelled in terms of the sum of the thickness of each laminate combined, this method has been found to have little effect on the ballistic properties of the composite model. An example of the standard model geometry and mesh can be seen in Fig. 2.
B. Material Model

Modelling impacts at the meso-mechanical scale is the most common method when considering composites of a laminate structure and although these models do not define the constituents themselves, they make the problems computationally tractable whilst maintaining the right proportions and physical properties to correctly simulate impacts. Due to the complex nature of composite material models the key equations governing the material behaviour are explained, however, the material parameters specific to this model are not listed for brevity. These parameters can be found in the work by Long Nguyen along with a detailed explanation of the source and validation of each value.

Using discrete sub-laminate layers with orthotropic material properties as to account for the anisotropic behaviour of UD ply it was possible to introduce delamination effects as a form of energy absorption and failure. Autodyn uses a breakable contact algorithm combining normal and shear stress to approximate the bond between lamina as represented in Eq. (5).

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

The exponents \(a\) and \(b\) are constants to relate the combined loading of shear and normal stress, which in the absence of this relation was set to one. A benefit of discretising the model into sub-laminates is that the through thickness tensile failure is represented by delamination and can thus be separated from the failure model. In the modified Hashin-Tsai failure model used by Nguyen et al. damage prediction in the through thickness direction is described by a relationship between the through thickness tensile strength of Dyneema and the through thickness shear strength. Unmodified, this relationship can cause premature shear failure due to the damage propagation caused by weak tensile strength. To resolve this problem the through thickness tensile strength was set to infinite and decoupled from the damage criterion, the equations used for this method by Autodyn are shown in Eq. (6) and Eq. (7).

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

\[
D_{ii} = \left(\frac{L_{ii}\sigma_{if}^*}{2G_{ij}}\right)
\]  

By disabling through thickness tensile failure instabilities were introduced, however, Nguyen et al. resolved these by using a shock Mie-Gruneisen Equation of State (EoS) in combination with the Orthotropic EoS. This is feasible as the shock Mie-Gruneisen model defines the same material response in compression and expansion and the actual response is more realistically represented through delamination. To correctly model the yield and strain hardening of Dyneema the material strength model was also set to orthotropic, in which the yield surface is defined by Eq. (8).

\[
f(\sigma_{ij}) = a_{11}\sigma_{11}^2 + a_{22}\sigma_{22}^2 + a_{33}\sigma_{33}^2 + 2a_{12}\sigma_{11}\sigma_{22} + 2a_{23}\sigma_{22}\sigma_{33} + 2a_{31}\sigma_{33}\sigma_{11} + a_{44}\sigma_{12}^2 + a_{55}\sigma_{23}^2 + a_{66}\sigma_{31}^2 = k
\]  

Here the nine plasticity constants shape the yield surface dependant on the degree of anisotropy of the material. The factor \(k\) is set dependant on a 10 piecewise multi-linear hardening slope for effective stress versus effective strain which can be found by the summation of the normal and shear components in Eq. (9) and Eq. (10) respectively.

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

\[ \bar{\sigma} = \sigma_{ij} \sqrt{\frac{3a_{ij}}{2}} \quad \text{and} \quad \bar{\varepsilon}^p = \frac{\varepsilon_{ij}}{\sqrt{3a_{ij}}} \]  

The final component to the standard model was the inclusion of a custom damage based erosion subroutine from Nguyen et al. in which any cell that reached complete damage was eroded.\(^2\) This damage comes from the crack softening based failure described by Eq. (7). An element is eroded when \(D_{22} \text{ or } D_{33}\) is equal to one. In most computational models, erosion is based on a geometric strain limit as to prevent the excessive distortion of the cells. Large distortions can affect the computational efficiency of the simulation as solvers such as ANSYS Autodyn\(^{10}\) calculate the solution time step as a fraction of the time it takes for a wave to propagate through the smallest element in the model.\(^17\) In this case, the erosion model removes failed elements that are irrelevant to the remainder of the solution as they lose all strength following complete damage. Overall the model presented by Nguyen et al. contains “orthotropic coupling of the material volumetric and deviatoric responses, a non-linear EoS, orthotropic hardening, combined stress failure criteria and orthotropic energy-based softening.”\(^{10}\) The combination of these material properties and the accuracy of their models have made them the starting point for validating the thermally dependent meso-mechanical model created in this work.

C. Manual Thermal Softening

As mentioned, the work by Long Nguyen utilised a manual thermal softening of the sub-laminate model to introduce shock and plastic work heating effects.\(^{14}\) This method was replicated in order to establish a baseline set of values for the comparison of this work to existing numerical and experimental results. Using the temperature profile from the sub-laminate layers involved in tensile failure, a decrease in the in-plane tensile limits, Young’s Moduli and an increase in the plasticity constants \(a_{22}\) and \(\sigma_{33}\) was set proportional to a 35°C temperature rise. The results of the 36 mm standard model and manually thermally softened model are compared later. Despite being an effective means to approximate the thermal softening effects, it requires a time consuming process of simulating an impact condition to extract the temperature profile to then repeat the simulation with the thermally softened back layers. This method doubles the processing time per test case in order to improve the precision of the model’s \(V_{BL}\) prediction. By creating a custom subroutine it was possible to remove this computational expense by making the thermal softening an automated process.

D. Subroutines

In order to create automatic thermal softening in the UHMW-PE model, a custom subroutine was required that could update the material parameters of each cell based on the local element temperature. The software packages Microsoft\(^{\circledR}\) Visual Studios 2010 and Intel\(^{\circledR}\) Professional Fortran Compiler 2013 were used to create the custom subroutines and compile them into Autodyn\(^{\circledR}\) executables. These software packages enabled the modification of the existing erosion model used by Nguyen et al. along with the creation of a new subroutine for the application of thermal softening. Through a series of attempted subroutines it was found that the Autodyn\(^{\circledR}\) program could not support a cell by cell update of material parameters. This is due to the way in which the properties are stored and called from a single material data table that is referenced throughout the simulation. If multiple attempts to edit this data were made in a single cycle of the hydrocode, the program could not accept the data and would either corrupt the model or shutdown. Following this limitation, an alternate method was used in which the damage criteria from Eq. (6) and Eq. (7) was modified to incorporate a thermal softening component. The damage parameter \(D_{ij}\) and the crack strain \(\epsilon_{cr}\) were both coupled to a thermal factor to achieve this model. The factor was based on an IF ELSE statement in which for a temperature below ambient (293 K) the factor was zero, and above melting (425 K) the factor was one, with a linear interpolation between. This was set to match the near linear decrease in Young’s Modulus and tensile strength between these temperatures. This proved impractical after a series of tests were conducted in which, regardless of the temperature sensitivity, over-penetration occurred in every model. The final set of subroutines produced utilised a combination of the initial method and that from the work by Long Nguyen.\(^{14}\) The initial standard model was modified as to ensure each sub-laminate layer was defined as a unique material. By this method the subroutine could be called at the end of each cycle to iterate through each sub-laminate layer and modify the material parameters based on the average or maximum temperature. This method prevented the corruption of the model and allowed for the temperature sensitivity to be calibrated to yield more precise results when compared to experimental values.
IV. Results and Discussion

The residual velocity predictions obtained from the 36 mm standard model and the manually thermally softened model are compared in Fig. 3.

![36mm Target Impact vs Residual Velocity Plots](image)

**Figure 3.** Numerical residual velocity predictions for 36 mm Dyneema® HB26 target impacted by 20 mm FSP compared to numerical results and experimental $V_{50}$ courtesy (L Nguyen. 2015).

Using the Lambert-Jonas (L-J) curve fit for both models, the results obtained in this work were in good agreement with those produced by Long Nguyen. This method proves the validity and the sensitivity of the material model to temperature effects. The theoretical $V_{BL}$ was decreased by more than 175 m/s to more closely match the experimental $V_{50}$ of 901 m/s with only a 35°C increase in temperature to a third of the models thickness. From these results it was possible to move forward and compare the validity of the above mentioned subroutines under a range of thicknesses and impact velocities. Using the final concept for a temperature dependent subroutine a number of thermal softening factors were tested for 10 mm, 20 mm and 36 mm targets. Initially a 100% thermal dependence was tested in which the material Young’s Moduli and tensile strengths followed the trend in Fig. 1. Although this factor best represented the physical behaviour of Dyneema® the solver was unable to achieve a converged solution when the Young’s Moduli approached zero. As such a 90% and 80% dependence were tested to match as close as possible to this trend, however, both proved invalid due to the excessive shear plugging ratio when simulating the 36 mm target. As the top layers physically undergo melt damage and shear failure it is expected that they should penetrate easier when thermal softening effects are incorporated, however, in combination with the damage based erosion model, failure was prematurely initiated. From the orthotropic softening model described in Eq. (6) and Eq. (7) a decrease in the tensile limit immediately induced crack strain once any principle stress exceeded the reduced limit. As such, the material directly under the point of impact was heated above melting, softened, damaged and then eroded all without sufficient loss of FSP kinetic energy. The flow on effect was that subsequent layers were excessively heated due to the projectile still maintaining sufficient velocity until the entire model failed prematurely to melt damage. A 70% thermal softening factor was the largest used that was able to achieve realistic results. Initially this factor used the average temperature over the sub-laminate layer, however, as can be seen from the results in Fig. 4 this factor still over predicted the $V_{BL}$. All subsequent models tested that yielded reasonable results were based on a factor of the maximum temperature in the sub-laminate layer. The final modification to the subroutine was to add a strain based failure component to the existing damage based erosion model. By this means, damage alone could not induce erosion as it had to be coupled with either an effective strain or effective plastic strain proportional to the element’s temperature. This method was used to model the increase in strain to failure shown in Fig. 1. The intent of this addition was to prevent the excessive shear plugging of the thinner targets that was predicted in the thermally softened models. In combination with this custom erosion model further thermal softening factors were tested until an acceptable solution was found for all target thicknesses tested. The results from the 36 mm simulations are shown in Fig. 4.

It was established that with the custom erosion criteria and a 50% thermal softening component, the model was able to align the theoretical $V_{BL}$ to the experimental $V_{50}$. It is not sufficient, however, for the
model to only comply with the 36 mm target results, for this technique to be used as a predictive tool for new armour designs it must be validated against a range of target thicknesses. In the case of the 10 mm and 20 mm targets, thermal softening was reported to only account for 1% error, as such, it was expected that with the thermal softening model applied, little change should be seen in their ballistic limit velocity predictions. The residual velocities for all tests using this final subroutine are plotted against the standard model and the existing experimental data in Fig. 5.

The residual velocity of the 10 mm and 20 mm targets were in good agreement with the experimental values and were almost identical to those values obtained by the standard model. As stated this is a desired result, as the thinner targets impacted at lower speeds were not expected to be effected by thermal factors. Not seen from these plots however is the difference in damage and failure of the models when the thermal softening factor is applied. Beyond the $V_{BL}$ predictions the deformation of the models must match that expected from the physical experiments. The qualitative validity of the standard model has been extensively proven in Long Nguyen’s work, demonstrating the various material failure modes seen in the experimental samples. The thermally softened models maintained these failure modes including fibre pull in, the formation of sub-laminate layers, delamination, sub-laminate folding, shear plugging and back-face bulging. These failure modes are highlighted in Fig. 6.

The temperature contours in Fig. 6(b) emphasises the areas affected by melting and thermal softening. This allows for an added level of penetration which causes fewer sub-laminate layers at the rear of the target to absorb the kinetic energy through tensile failure, leading to an increase in the speed of propagation of the apex position. In both the 20 mm and 36 mm test cases impacted below the ballistic limit velocity, the apex positions are over predicted compared to the experimental results, however, they are more accurately
predicted through the custom subroutine than either the standard model or the manually thermally softened model. The results for these tests are plotted in Fig. 7(b) and Fig. 7(c). It must be mentioned that the 20 mm apex position prediction in the work by Nguyen et al. was near exact compared to the experimental results, whilst the results obtained in this work initially significantly over predicted the apex position. In the 10 mm case there is a slight over prediction to the original model as can be seen in Fig. 7(a).

The over prediction of the apex position is a consequence of the method used to introduce thermal softening. As the entire sub-laminate layer is softened based on a percentage of the maximum temperature, immediately after impact, the first few layers are completely thermally softened whereas physically, this thermal softening is only localised to the point of impact. In both the 20 mm and 36 mm cases, this problem is overcome by the fact that these layers play little part in the tensile failure once they have been penetrated through shear plugging. In the case of the 10 mm target, no shear plugging is experienced below the ballistic limit; hence the temperature immediately following impact directly affects layers involved in tensile failure, leading to a less precise prediction of the apex position when compared to the standard model. Despite this effect in the 10 mm targets, the custom subroutine better predicts the $V_{BL}$ and maintains the qualitative deformation whilst improving the quantitative precision in thicker targets under higher impact velocities.
V. Conclusions

Thermal softening and melting effects have been shown in previous works to account for a large degree of error in the prediction of UHMW-PE composite ballistic limit velocities. Through the creation of a custom subroutine the thermal softening effects of UHMW-PE have been adequately modelled in a manner that both saves computational processing time and increases precision to physical experimental test results. Through the analysis outlined in this report, the concept has been proven that it is possible to model these effects by modifying existing material models inbuilt to Autodyn® through custom subroutines. Further it has been demonstrated that although thermal softening of the entire layer causes a slight over prediction of the deformation compared to experimental test cases, for thicker targets the damage is more accurately predicted than through the previous standard models or manually thermally softened models.

VI. Recommendations

True prediction of UHMW-PE response to ballistic and hypervelocity impact can only be achieved with a comprehensive model that involves all aspects of the material failure. This work is an introduction to an automated thermal softening model and requires extensive experimental testing to be validated in all ranges of operating conditions. It is recommended in order for this work to be carried forward that a series of impact tests be conducted using the TSLGG to characterise the response and ballistic limit of Dyneema® HB26 under varying ambient temperatures. For any new armour made from an UHMW-PE composite it is likely that the operating conditions could vary as greatly as from 0°C to 50°C and as such it is critical that material models can incorporate the effects of the ambient conditions in combination with the further thermal effects from shock heating and plastic work. This can only be achieved by recalibrating the custom subroutine to match the material response found through such future experimentation.

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