SIMULATION OF HYPERVELOCITY DEBRIS IMPACT AND SPACECRAFT SHIELDING PERFORMANCE

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ABSTRACT

The objective of the work presented in this paper is the simulation of hypervelocity impact on aluminium-carbon/epoxy-aluminium shields; such multi-layered arrangement is being used by the European Columbus module of the International Space Station. In addition, thermodynamically consistent material models are introduced for each component of the multilayered array which yields a more accurate physical representation of the material response to high velocity impact loading.

Thermodynamically consistent models for aluminium and carbon/epoxy are proposed. In order to describe material behavior under high-intensity loadings a 2-D anisotropic elasto-plastic constitutive model coupled with a damage tensor ω_{ij}, an equation of state, and a failure criterion (based on the critical value of a specific entropy function expressed in terms of the dissipation function) have been developed. The model includes the following key aspects of material response to hypervelocity impact: non-linear anisotropic strength, shock effects and associated energy dependence, compaction, compressive and tensile failure and strain rate effects.

The severe deformations occurring in any hypervelocity impact event are best described by meshless methods since they offer clear advantages for modelling large deformations and failure of solids when compared to mesh based methods.

The simulations presented here are the result of the application of the Smoothed Particle Hydrodynamics (SPH) method to the impact and penetration problem and the incorporation of thermodynamically consistent material models into the Cranfield University SPH solver.

1. INTRODUCTION

Novel approaches to designing advanced aerospace systems require evaluation of extreme operating conditions and an assessment of different failure scenarios and their prevention. The hypervelocity impact of space debris and micrometeoroids is a potential threat to spacecraft which requires careful consideration if structural and sub-system integrity is to be maintained throughout the intended spacecraft mission. Current space debris shields can be effective against small particles of up to 1 cm in size. Weight effective debris shields against particles larger than 1cm are not technically feasible. Fragments larger than 10cm are ground-tracked so that the collision probability with the spacecraft is known and avoidance maneuvers can be performed when required.

Passive shielding on space structures has become a key component in structural design as mission duration, and hence the exposure to space debris and micrometeoroids, has been extended over the years. Designing effective protection requires a good understanding of impact phenomena and the development of new techniques for analysing structures and materials. Nowadays multilayered composite structures are commonly used in spacecraft shielding configurations in order to minimise the risk of subsystem failure and potential total loss of the spacecraft.

The use of accurate material models and robust numerical solvers results in a more effective, fast and accurate design process. In the present study thermodynamically consistent material models are introduced for each component of the multi-layered shield array. Additionally, the application of the Smoothed Particle Hydrodynamics (SPH) method to the impact and penetration problem and the incorporation of consistent material models into the Cranfield University SPH solver allows for a more accurate representation of the material response under severe deformations.

This study concentrates on the numerical simulation of debris impact onto a multi-layered shield arrangement. The geometry, materials and dimensions are similar to those typically found in modern spacecraft Hayhurst et al. (1999), Thoma et al. (2004). The overall thickness of the multi-layered arrangement considered is 120.00mm and the materials considered are 7075-T6 aluminium alloy for the front bumper shield and the back wall and carbon/epoxy composite for the

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intermediate shield. The characteristics of debris impact differ in low earth orbits and geostationary orbits. In GEO, impact velocities range from a few hundred meters per second to just over one kilometre per second Hayhurst et al. (1999), Thoma et al. (2004) whereas in LEO the impact velocity has been estimated to be as high as ten kilometres per second Katayama et al. (1997). An impact velocity of 1.5Km/s is chosen to carry out the numerical experiment.

2. CONSTITUTIVE EQUATION

2.1. Damageable thermo-elasto-visco-plastic medium.

The system of constitutive equations for modelling the damageable thermo-elasto-visco-plastic medium is as follows:

$$\varepsilon_{kk} = \frac{\sigma}{K} + \alpha_v (T - T_0) + \Lambda_1 \int_0^{\omega} \frac{\partial \varphi}{\partial \sigma} d\omega , \quad (1)$$

$$e_{ij}^e = \frac{S_{ij}}{2\mu} + \Lambda_2 \int_0^{\alpha} \frac{\partial \psi}{\partial \sigma_{ij}} , \quad (2)$$

$$\dot{\varepsilon}_{ij}^p = \frac{S_{ij}}{2\eta} \frac{S_u - \sqrt{\frac{2}{3}}\, Y}{S_u} H(S_u - \sqrt{\frac{2}{3}}\, Y) \quad (3)$$

$$\dot{\omega} = \varphi(\omega, \sigma) = B\left(\frac{\sigma}{(1-\omega)(1-\alpha)} - \sigma_*\right) H\left(\frac{\sigma}{(1-\omega)(1-\alpha)} - \sigma_*\right) +$$

$$+ \omega \frac{\sigma - \sigma^+}{4\eta_0} H(\sigma - \sigma^+) + \omega \frac{\sigma - \sigma^-}{4\eta_0} H(\sigma^- - \sigma) , \quad (4)$$

$$\sigma^+ = -\frac{2}{3} Y_0 \ln \omega , \sigma^- = -\sigma^+ ,$$

$$\dot{\alpha} = \psi(\omega, \alpha, S_u) = C\left(\frac{S_u}{(1-\omega)(1-\alpha)} - S_u^*\right) H\left(\frac{S_u}{(1-\omega)(1-\alpha)} - S_u^*\right)$$

$$\eta = \eta_0 (1-\omega)(1-\alpha) , \; Y = Y_0 (1-\omega)(1-\alpha) , \quad (5)$$

$$\rho c_\sigma \dot{T} + \alpha_v \dot{\sigma} T = S_{ij} \dot{\varepsilon}_{ij}^p + \Lambda_1 \dot{\omega}^2 + \Lambda_1 \dot{\alpha}^2 - div\, \vec{q} , \quad (6)$$

$$\vec{q} = -\kappa\, grad\, T , \; S_u = \sqrt{S_{ij} S_{ij}} . \quad (7)$$

Here $\sigma_{ij}, \varepsilon_{ij}^e, \varepsilon_{ij}^p$ are the components of the stress tensor, elastic and non-elastic (viscoplastic) deformation tensors, respectively ($\varepsilon_{ij} = \varepsilon_{ij}^e + \varepsilon_{ij}^p ; \dot{\varepsilon}_{kk}^p = 0$); T is the absolute temperature; $\vec{q}$ is the heat flux; ρ is the density; B, C, Λ_1, Λ_2, σ_*, S_u^* are the material constants connected to the damage parameters ω and α; K_0, μ_0, η_0, Y_0 are the bulk modulus, shear modulus, dynamic viscosity and static yield limit of plasticity for an undamaged material respectively; c_σ is the heat conductivity at constant stress; α_v is the coefficient of volume expansion; κ is the coefficient of heat conduction; $H(x)$ is the Haviside function; the dot over the symbols indicates the material derivative with respect to time.

The kinetic equation for the volume damage ω consists of three terms. The first one has the form of the Tuler – Bucher (1968) equation and describes the formation and initial growth of the volume damage ω. Then, as ω gets accumulated, the second term describing the viscous growth in domains of tension of the material comes into play Kiselev and Lukyanov (2002). The third term describes the viscoplastic flow in pores when the material is compressed. Note that the equation for ω was taken from the dynamic problem on a single spherical pore of inner radius a and outer radius b in a viscoplastic incompressible material. This model is an extension of the Socolovsky– Malvern type elastoviscoplastic medium and takes into account the formation and accumulation of damage in domains of intensive tension, their disappearance under compression as well as the heating effects and the accumulation of damage under shear deformation. The mechanical, structural, and heat processes are mutually dependent.

The evolution of the intensive plastic flow and accumulation of micro-structural damage may be considered as a process of pre-fracture of the material. The entropy criterion of limiting specific dissipation Kiselev and Lukyanov (2002):

$$D = \int_0^{t_*} \frac{1}{\rho} (d_M + d_F + d_T) dt = D_* \quad (8)$$

$$d_M = S_{ij} \dot{\varepsilon}_{ij}^p , d_F = \Lambda_1 \dot{\omega}^2 + \Lambda_2 \dot{\alpha}^2 ,$$

$$d_T = \kappa \frac{(grad\, T)^2}{T}$$

is proposed as the criterion for the beginning of macrofracture (i.e., the beginning of formation of cracks or new free surfaces in the material). Here t_* is the time at the beginning of fracture; D_* is a material constant (the limiting specific dissipation); d_M, d_F and d_T are the mechanical dissipation, the dissipation of continuum fracture and the thermal dissipation respectively.

2.2. Modelling composites

For metals there is exhaustive information in the literature on dynamic material properties under large strains and high strain rates and appropriate constitutive equations have been used for structural impact simulations. For composite materials, dynamic failure behavior is very complex due to the different fibres and matrices available, the different fibre reinforcement types, the possibility of fibre dominated or matrix dominated failure modes, and the rate dependence of the polymer resin properties. Thus at present there are no universally accepted material laws for crash and impact simulations with composites. It was considered that a homogeneous orthotropic elastic damaging material was an appropriate model for fabric laminates as this is applicable to brittle materials whose properties are degraded by micro-cracking. The model presented in this paper is macro-mechanically based and suitable for implementation into a numerical code. Each layer/weave of the material was not modelled explicitly but represented by an equivalent volume with properties at a macro scale which are representative of the combined micro-mechanical material response under the loading conditions considered. Constitutive laws for orthotropic elastic materials without internal damage parameters are expressed as:

$$\begin{bmatrix} \varepsilon_{xx} \\ \varepsilon_{yy} \\ 2\varepsilon_{xy} \end{bmatrix} = \begin{pmatrix} \frac{1}{E_x} & \frac{-\nu_{xy}}{E_x} & 0 \\ \frac{-\nu_{xy}}{E_x} & \frac{1}{E_y} & 0 \\ 0 & 0 & \frac{1}{G_{xy}} \end{pmatrix} \begin{bmatrix} \sigma_{xx} \\ \sigma_{yy} \\ \sigma_{xy} \end{bmatrix} \tag{9}$$

where ν_{xy} is the principal Poisson's ratio. Eq. (9) can be rewritten in the following form for materials which incorporate an internal damage tensor:

$$\sigma_{(ij)} = C_{(ij)kl}(\omega_{(ij)})\varepsilon_{kl} \tag{10}$$

where

$$\omega_{pq} = \begin{pmatrix} \omega_x & \omega_{xy} \\ \omega_{xy} & \omega_y \end{pmatrix},$$

$$C_{(ij)kl}(\omega_{(ij)}) = C^0_{(ij)kl} \cdot (1-\omega_{(ij)}) \tag{11}$$

where $C^0_{(ij)kl}$ is the undamaged stiffness matrix.

This general plane stress form for an orthotropic elastic material with damage has 3 scalar damage parameters $\omega_x, \omega_y, \omega_{xy}$ and 4 "undamaged " elastic constants: Young's moduli in the principal orthotropy directions E_x, E_y, the in-plane shear modulus G_{xy}, and the principal Poisson's ratio ν_{xy}. The damage parameters have values $0 \le \omega_i \le 1,\ i = x, y, xy$ and represent modulus reductions under different loading conditions due to progressive damage within the material.

For dissipation of continuous (distributed) fracture the following equation of state is assumed:

$$-\rho\frac{\partial F}{\partial \omega_{ij}} = A_{ijkl}\dot{\omega}_{kl}, \tag{12}$$

$$d_F = A_{1111}\dot{\omega}_x^2 + 2A_{1122}\dot{\omega}_x\dot{\omega}_y + A_{2222}\dot{\omega}_y^2 + 2A_{1212}\dot{\omega}_{xy}^2$$

where $F = F(\tilde{\varepsilon}, T, \omega_{ij})$ is the free energy of the composite material, A_{ijkl} is the matrix of Onsager's parameters.

Eq. (12) establishes a linear functional dependence between the thermodynamic force and the thermodynamic flux in accordance with Onsager's theory. Therefore, dissipation of continual fracture is non negative $d_F \ge 0$.

The basic thermodynamic equation which can be used for defining the constitutive equation has the following from:

$$\tilde{\sigma} = \rho\frac{\partial F}{\partial \tilde{\varepsilon}^e},\ s = -\frac{\partial F}{\partial T} \tag{13}$$

Using Eq. (13) and following the derivation described in Lukyanov (2004), the following constitutive relation for the composite material can be derived:

$$\sigma_{ij} = C_{ijkl}(\tilde{\omega})\varepsilon_{kl} + C_{ijkl}(\tilde{\omega})\omega_{pr}A_{prmn}\frac{\partial \dot{\omega}_{mn}}{\partial \sigma_{kl}}, \tag{14}$$

The last step in the generation of the constitutive equations for composite materials involves the introduction of expressions which describe the evolution of the rate of damage tensor:

$$\dot{\omega} = \chi(\omega_{ij}, \sigma_{ij}) = B^t_{ij}\left(\frac{\sigma_{ij}}{(1-\omega_{ij})} - \sigma^{ij}_t\right)H\left(\frac{\sigma_{ij}}{(1-\omega_{ij})} - \sigma^{ij}_t\right) + B^c_{ij}\left(-\sigma^{ij}_c - \frac{\sigma_{ij}}{(1-\omega_{ij})}\right)H\left(-\sigma^{ij}_c - \frac{\sigma_{ij}}{(1-\omega_{ij})}\right) \tag{15}$$

where B^t_{ij}, B^c_{ij} are scale factors for the tensile and compression cases respectively and $\sigma^{ij}_t, \sigma^{ij}_c$ are absolute critical values of stress for the tensile and compression cases respectively.

The entropy criterion of limiting specific dissipation Kiselev and Lukyanov (2002):

$$D^C = \int_0^{t_*} \frac{1}{\rho}(d_F)dt = D_*^C \qquad (16)$$

is proposed as the criterion for the onset of macro-fracture (i.e., the beginning of formation of cracks (new free surfaces) in the material). Here t_* is the time at the start of fracture; D_*^C is a composite material constant (the limiting specific dissipation); d_F is the dissipation of continuum fracture for composite material.

3. SPH METHOD

Smoothed Particle Hydrodynamics (SPH), Lucy (1977), provides an excellent tool for simulating the physics of dynamic events in solid mechanics that involve large deformations of the domain Belytschko and Xiao (2000), Libersky and Petschek (1990). It is particularly well suited to problems that result in complex fracture paths. Conventional SPH is a robust method that has been successfully implemented to simulate dynamic fracture and fragmentation of solids Libersky and Petschek (1990).

SPH is a Lagrangian particle method that uses no underlying mesh. The absence of a mesh and the calculations of interactions among particles based on their separation alone means that large deformations can be computed with relative ease. SPH relies on interpolation theory where the value of a field can be approximated by smoothing functions (or kernels) which are non-zero in the particles' sub-domains. Additionally, through SPH the kernel approximation allows spatial gradients to be determined from the values of the function and the first spatial derivative of the kernel rather than the derivatives of the function itself. Several forms of kernels can be employed to construct SPH approximations Belytschko and Xiao (2000).

The conventional approach used to form SPH equations is as follows:

1st step.- Estimate the kernel. For a vector function *f* at a point, whose position vector is *x*, in an interval Ω, the conventional kernel estimate is given by the integral interpolant:

$$\langle f(x)\rangle \approx \int_\Omega f(x')W(x-x',h)dx' \qquad (17)$$

where W is a kernel function and depends upon two variables, $|x-x'|$ and *h* a width control parameter (also known as smoothing length).

The kernel function must satisfy the following requirements:

1.- $W(x-x',h)=0$ when $|x-x'|\geq kh$ (i.e. the Kernel should exhibit compact support). *k* is a scale factor that determines the supporting area of the smoothing function.

2.- $W(x-x',h)\geq 0$ in the compact support area where $|x-x'|\leq kh$

3.- Integration of W over the entire domain is unity

$$\int_\Omega W(x-x',h)dx' = 1 \qquad (18)$$

To these requirements we can add that the kernel has to be differentiable at least once, the reason being that the kernel approximation allows spatial gradients to be determined from the values of the function and the first spatial derivative of the kernel rather than the derivatives of the function itself. Additionally, the derivative should be continuous to prevent large fluctuations in the values of the variables of particle *i*.

4.- The limit of W equals the Dirac delta function as *h* approaches zero.

$$\lim_{h\to 0} W(x-x',h) = \delta(x-x') \qquad (19)$$

2nd step.- The second step is to convert the kernel integrals into a volume weighted sum. This is known as particle approximation. Thus:

$$f_i = f(x_i) = \langle f(x)\rangle \approx \sum_{j=1}^{N} f_j W_{ij}\frac{m_j}{\rho_j} \qquad (20)$$

In the equation above the subscript *i* and *j* denote particle number, m_j and ρ_j the mass and the density of particle *j*, *N* the total number of particles in the field and $W_{ij} = W(x_i - x_{j,} h)$ is the smoothing function. It is important to realise that although the summations are carried out over the entire number of particles *N*, only a small number contribute since one of the properties of the kernel is that it has compact support (i.e. its value falls off rapidly as $|x_i - x_j| \geq h$).

Neglecting thermomechanical and frictional forces, the conservation equations in differential form can be converted to their respective discretised form.

To illustrate how this method works we apply the steps above to obtain an expression for the velocity gradient and the momentum equation in an SPH framework:

$$\mathbf{L}_i = -\sum_{j\in\mathbf{S}} (\mathbf{v}_j - \mathbf{v}_i)\otimes \nabla\mathbf{W}_{ij}\mathbf{V}_j \qquad (21)$$

$$\mathbf{a}_i = -\sum_{j \in \mathbf{S}} m_j \left(\frac{\sigma_i}{\rho_i^2} + \frac{\sigma_j}{\rho_j^2} \right) \nabla \mathbf{W}_{ij} \tag{22}$$

Where the subscripts i and j correspond to the home and neighbor particles respectively, ∇ is the differential operator expressed in material coordinates, $\mathbf{W}_{ij}$ is the smoothing function, σ_j is the Cauchy stress tensor at particle j, ρ_j, m_j, $\mathbf{V}_j$ are the current particle density, mass and volume respectively v_j is velocity at particle j and $\mathbf{a}_i$ is the particle acceleration, S is the particle support. Equation (21) is key for computing the rate of deformation tensor which in turn is used for updating stresses in the continuum.

4. NUMERICAL SIMULATION

The 2-D numerical model considered in our study consists of a projectile, a bumper shield, a secondary shield and a back wall. The initial set up is depicted on Figure 1.

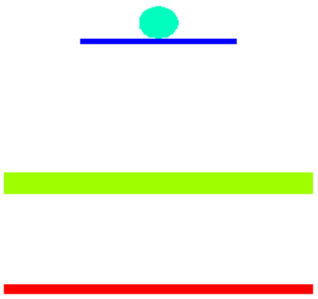

Figure 1. 2-D numerical model set up.

The overall thickness of the multi-shield arrangement is 120.00 mm measured from the rear face of the bumper shield to the front face of the back wall, the bumper shield thickness is 2.50 mm, the secondary shield thickness is 10.00mm and the back wall thickness is 5.00mm. The separation between the bumper shield and the secondary shield is 70.00mm. The material used to simulate the bumper shield and back wall is Al 7075-T6 since this is an alloy commonly employed in aerospace applications. The secondary shield consists of a multilayered arrangement of carbon/epoxy layers and the projectile is a 15 mm diameter Al 1100-O sphere.

Simulations were run with the impactor moving at 1.5Km/s. The velocity field plot corresponding to the 1.5Km/s case is shown in Figure 2.

Whereas the momentum transfer between the projectile and the portion of the shield which disintegrates is evident (Figure 2), the rest of the shield remains largely intact due to the high speed at which the impact and penetration process occurs.

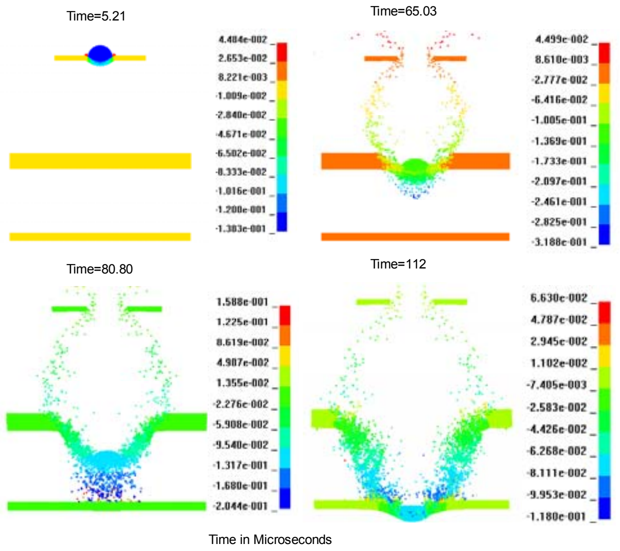

Figure 2. Sequence of a 1.5Km/s impact at normal incidence to the bumper plate.

At this speed the back wall suffers great damage with the consequent potential loss of the spacecraft. The implementation of the SPH method allows the debris cloud and its effect on a subsequent plate to be modelled as one continuous problem. Figure 3 shows a plot of the fracture propagation (blue particles indicate fractured particles) within the multi-layer arrangement components corresponding to a 1.5km/s impact.

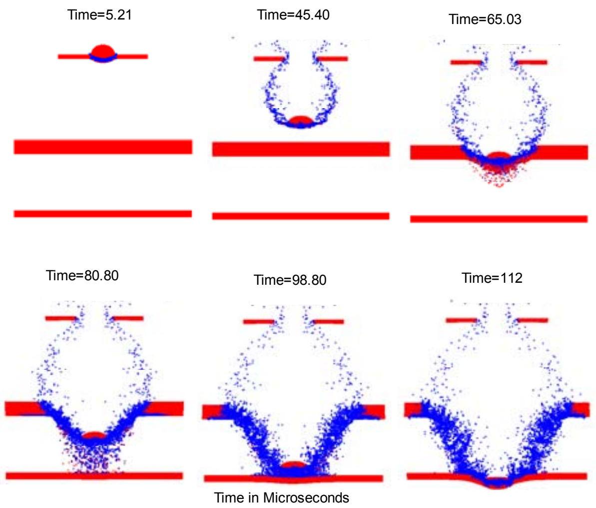

Figure 3. Damage distribution within the material.

From figure 3 we can see that most of the particles which participate in the debris cloud are either damaged or failed. The percentage of the particles which are damaged but not failed is relatively small which lead us to think that the contribution of tensile instability in the fracture process is negligible; it can thus be concluded that the main fracture mechanism is induced by the introduction of a thermodynamically consistent damage model and not by numerical instability.

5. CONCLUSION

The simulation of hypervelocity impact on aluminium-kevlar/epoxy-aluminium shields at 1.5Km/s has been introduced in this paper. In addition, thermodynamically consistent material models have been introduced for each component of the multilayered array with incorporated specific entropy as the criteria of fracture origination which allows more accurate physical representation of the material response to high velocity impact loading. In this paper it has been demonstrated that material failure originates and propagates mainly as a result of the introduction of a consistent constitutive model of damageable media. However, some numerical fracture due to the tensile instability inherent to the basic SPH method is also present. A thermodynamically consistent development of the composite constitutive model has also been introduced. This model includes some non-standard parameters which were obtained through comparison of dynamic tensile tests with numerical simulations of the same test. Most composite materials show strain rate dependent properties. The composite material model presented in this paper does not take into account strain rate dependency. This problem is in the scope of further research and will be published in forthcoming papers.

The nonstandard material constants for metals introduced by Kiselev and Lukyanov (2002) showed good agreement with experiments and the present work is a direct extension of their results into a 2-D case. The physics of the damage model were implemented into the SPH solver which allowed the simulation of complex fracture paths and debris clouds. It is important to note that the value of damage alone does not constitute a reliable means of predicting fracture as it varies significantly amongst particles where fracture has occurred. The results presented here do not take into account the contribution of the second damage parameter α into the fracture process for metals. This parameter plays a major role in the origination and propagation of shear bands. A forthcoming paper will deal with the complete set of damage parameters. The 2-D results shown provide an insight into the physics of the fracture process and will provide the foundation for a real 3-D simulation in future research.

6. REFERENCES

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