DEBONDING OF FACE/CORE INTERFACE OF A CIRCULAR SANDWICH PLATE DUE TO HAIL-LIKE IMPACT

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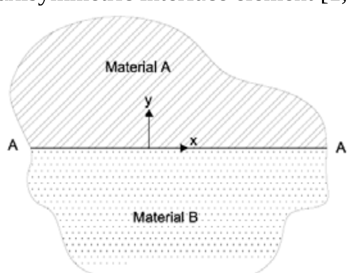
Summary Debonding of face/core interfaces of a circular sandwich plate under hail-like impact is considered, treating the plate as a 3-d axisymmetric structure, peripherally fixed and subjected to a central equivalent static load. The interface element of the constructed *Mathematica* FE program satisfies the stress-continuity at one interface point, thereby determining through the thickness stresses more accurately. Using an interface shear stress failure criterion, debonding radii are found for different impactor materials, sizes, and striking velocities.

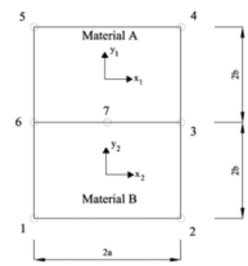
1. INTRODUCTION

One of the concerns in the design of any aircraft is the possibility of impact damage to the fuselage by low-mass high-velocity projectiles like runway debris, or impact by hailstones in a hailstorm. Such damage in metal face sandwich construction generally involves visible plastic indentations on the impacted sheet. When the sheets are high strength composites, the damage is generally more complex (involving fiber damage and delamination) and visually undetectable, because of the recoverable viscoelastic deformation. As a first step, the present investigation deals with the central impact on the simpler metal-face circular sandwich panels. The motivation is to simulate laboratory tests on impact of sandwich panels of circular geometry. The debonding of the faces from the core is considered as the main issue. For a spherical projectile of given radius, material, and striking velocity, the debonding radius is determined.

2. THE INTERFACE STRESSES AND DEBONDING MODELING

To address the debonding of sandwich plates, one must evaluate the interface stresses accurately. In the displacement based FE, although the displacement compatibility is generally ensured, continuity of the stress vector at the interface of two elements is not. As a result, non unique stress values are predicted for the components which ought to be unique. The differences are quite large for sandwich plates since the face/core ratio of the Young's moduli is quite high. To solve this problem, an axisymmetric interface element [1, 2] was implemented in the constructed FE program.





The axisymmetric bimaterial element is shown in a radial plane. Let xyz be the right handed axes, with x along the radius and y upward. The xz plane is the interface plane between the materials A and B. The displacements and the stress vectors on either side of the interface must be equal, $u^a = u^b, v^a = v^b, w^a = w^b$ and $\underline{T}^a = \underline{T}^b$. These require the following complementary forms

$$\begin{bmatrix} \varepsilon_{xx} & \varepsilon^*_{xy} & \varepsilon_{xz} \\ \varepsilon^*_{yx} & \varepsilon^*_{yy} & \varepsilon_{yz} \\ \varepsilon_{zx} & \varepsilon^*_{zy} & \varepsilon_{zz} \end{bmatrix} \text{and} \begin{bmatrix} \sigma^*_{xx} & \sigma_{xy} & \sigma^*_{xz} \\ \sigma_{yx} & \sigma_{yy} & \sigma_{yz} \\ \sigma^*_{zx} & \sigma_{zy} & \sigma^*_{zz} \end{bmatrix}$$

of the strain and stress tensors. The un-starred components are required to be continuous whereas the starred ones are allowed to be discontinuous. The stresses and strains for each material are related by the respective linear isotropic law. The element is centered over the interface as shown. The 10×10 stiffness matrices $[K_a]$ and $[K_b]$ for material A and B, are obtained in the usual way by the radial and axial displacements of nodes 3, 4, 5, 6, 7 for A, and nodes 1, 2, 3, 6, 7 for B. The interface stress components σ_{nn}, σ_{nt} at node 7, computed for element A, and for element B, are now required to be equal, $[Q_a]\{\Delta_a\} = [Q_b]\{\Delta_b\}$ wherein $\{\Delta_a\},\{\Delta_b\}$ are column matrices of the dof (degrees of freedom) of elements A and B, and $[Q_a],[Q_b]$ each are 2×10 matrices. This equation can be used to express the 2 dof of node 7 in terms of the remaining 12 dof of the interface element. Thus one has $\{\Delta_a\} = [R_a]\{\Delta_c\}$ and $\{\Delta_b\} = [R_b]\{\Delta_c\}$ where $\{\Delta_c\}$ comprises of the 12 dof of the interface element. A virtual work argument then gives: $[K] = [R_a]^T[K_a][R_a] + [R_b]^T[K_b][R_b]$, the 12×12 stiffness matrix of the interface element. Compared against some exact solutions, the element fares extremely well [1].

Next, the debonding issue is addressed on the basis of a maximum interface shear stress criterion. As explained below, the kinetic energy of the projectile is converted into a central equivalent static load. Under this load, an initial FE analysis is performed for the intact plate. If the shear stress at any interface points exceeds the critical value, debonding is assumed to have occurred at those points. The debonding is modeled in the FE program by replacing the debonded element by the original two 5 node elements, meaning that the interface node is split in two. The split nodes are connected by short spring (gap) elements, and a second analysis is performed. A tension in the spring would indicate the split nodes moving apart, while a compression would mean their staying together. A third analysis is made with the gap elements assigned a high compressive stiffness but a negligible tensile one. The forces in these elements are again examined to see whether or not the assumptions about the split nodes are valid. If not, then a fresh analysis is made in accordance with the results. This iterative procedure is repeated until the results of consecutive analyses concur. After this convergence, if the criterion is not met at any interface point, the procedure is repeated until debonding stops.

3. EQUIVALENT STATIC LOAD

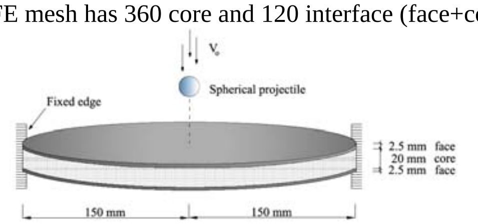
The analysis here follows [3]. The kinetic energy of a projectile hitting directly at plate centre is balanced by energies of bending-shear, membrane, and contact deformations, $MV_0^2/2 = E_{bs} + E_m + E_c$. This equation can be written as [2]:

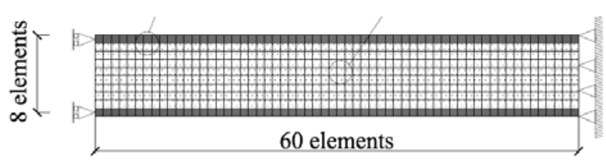
$$MV_0^2 = K_{bs}w^2 + K_m w^4/2 + (4/5)(K_{bs}w + K_m w^3)^{5/3}/n^{2/3}$$

where w is the maximum center deflection, and n is a stiffness coefficient dependent on material properties of the impactor and the plate. The coefficients n, K_{bs}, K_m can be calculated as in [2]. Thus, the deflection w is known for an energy input. The maximum static force is then $P = K_{bs}w + K_m w^3$, and contact radius $a_c = (3\pi P R_1/4)^{1/3}(K_1 + K_2)^{1/3}$ where R_1 is the radius of the spherical impactor, and the coefficients K_1, K_2 depend on the material properties [2]. As a conservative assumption, the analysis is adopted for the present problem by considering only the top face as effective.

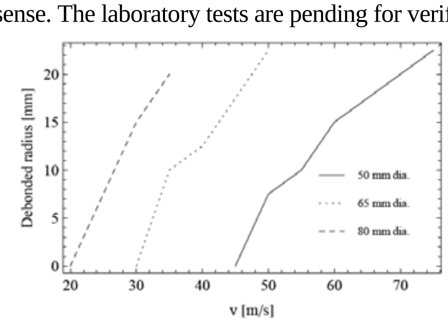
4. ANALYSIS EXAMPLE AND SOME RESULTS

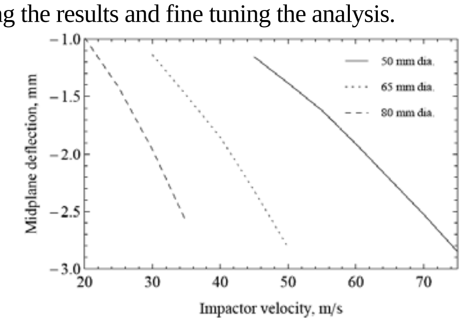
A circular sandwich plate of radius 150 mm, aluminum faces 2.5 mm thick and a PVC core 20 mm thick is chosen, with respective moduli $E_a = 70{,}000$ MPa, $\nu_a = 0.3$ and $E_c = 420$ MPa, $\nu_c = 0.32$ giving $E_a/E_c \approx 167$, a high value. The FE mesh has 360 core and 120 interface (face+core) QUAD elements, fixed periphery, and a central load P.





For a fully bonded plate, the results are similar to the analytical ones in [4] from a high order plate theory and no interface elements. For debonding from hail stones, ice impactors of 50, 65, 80 mm diameter were chosen. The moduli of ice were taken as $E_i = 10{,}000$ MPa, $\nu_i = 0.3$, and density $\rho_i = 920$ kg/m^3. From literature, a critical interface shear stress of 18 MPa was chosen as the debonding criterion. The figure below shows the calculated debonding radii as functions of the striking velocity for the three sizes of the ice balls. It is seen that the slope of these lines becomes smaller at higher striking velocities, meaning that debonding becomes harder at higher striking velocities. The centreline deflections, shown in the next figure, make sense. The laboratory tests are pending for verifying the results and fine tuning the analysis.





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