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Development of a user element in ABAQUS for modelling of cohesive laws in composite structures

Stefanie Feih
Abstract (max. 2000 char.):

The influence of different fibre sizings on the strength and fracture toughness of composites was studied by investigating the characteristics of fibre cross-over bridging in DCB specimens loaded with pure bending moments. These tests result in bridging laws, which are obtained by simultaneous measurements of the crack growth resistance and the end opening of the notch. The advantage of this method is that these bridging laws represent material laws independent of the specimen geometry. However, the adaption of the experimentally determined shape to a numerically valid model shape is not straightforward, and most existing publications consider theoretical and therefore simpler softening shapes. In this article, bridging laws were implemented into an interface element in the UEL user subroutine in the finite element code ABAQUS. Comparison with different experimental data points for crack opening, crack length and crack shape show the sensitivity of these results to the assumed bridging law shape. It is furthermore shown that the numerical predictions can be used to improve the bridging law fit. One shape with one adjustable parameter then fits all experimental data sets.
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1 Introduction

For glass fibre composites, the interfacial properties are controlled by the sizing, which is applied to the glass fibres during manufacture. For the same matrix system, a change of sizing results in changes of these properties, thereby influencing the mechanical properties such as strength and fracture toughness. The concept of strength is used for characterising crack initiation in composite design, while fracture toughness determines crack growth and damage development. In mode I crack growth in unidirectional fiber composites, fibre cross-over bridging occurs during cracking along the fiber direction. This failure mode plays an important role during delamination of fibre composites and splitting cracks around holes and notches. The fibre bridging zone must be modelled as a discrete mechanism on its own; failure is not just controlled by the cracking at the crack tip. The failure process can be described by a bridging law, which defines the relationship between the crack opening displacement and the local bridging tractions resulting from the bridging ligaments. Cohesive laws were measured experimentally in previous work. This report derives the necessary basics and equations to implement these laws into the commercial finite element code ABAQUS with a cohesive user element. Different numerical adjustments of the bridging law are discussed in detail. Crack aspects, such as crack opening shape and the influence of bridging law parameters, are studied based on the numerical results. It is furthermore of interest to identify the experimental measurements which show the highest sensitivity with respect to the bridging law shape.

2 Mode I bridging law measurement

The approach for the measurements of bridging laws is based on the application of the path independent $J$ integral [1], and has been used recently to determine the bridging characteristics of unidirectional carbon fibre/ epoxy composites [2] and glass fibre composites [3]. A symmetric DCB specimen is loaded with pure bending moments $M$ (Figure 1) under pure mode I. This specimen is one of the few practical specimen geometries, for which the global $J$ integral (i.e. the integral evaluated around the external boundaries of the specimen) can be determined analytically [1]:

$$J = 12(1 - \nu_{13}\nu_{31}) \frac{M^2}{b^2 H^3 E_{11}}$$  \hspace{1cm} (1)

$E_{11}$ is the Young’s modulus referring to the material directions, $\nu_{13}$ and $\nu_{31}$ are the major and minor Poisson’s ratio, $b$ is the width and $H$ the beam height.

![Figure 1. DCB specimen with pure bending moment](image)

Now consider the specimen having a crack with bridging fibres across the crack faces near the tip. The closure stress $\sigma$ ($x_2$-direction) can be assumed to depend only on the local crack
opening $\delta$, i.e. the crack grows in pure mode I. The bridging law $\sigma = \sigma(\delta)$ is then taken as identical at each point along the bridging zone. Since fibres will fail when loaded sufficiently, we assume the existence of a characteristic crack opening $\delta_0$, beyond which the closure traction vanishes. Shrinking the path of the $J$ integral to the crack faces and around the crack tip [4] gives

$$J = \int_0^{\delta^*} \sigma(\delta) d\delta + J_{\text{tip}},$$  \hspace{1cm} (2)

where $J_{\text{tip}}$ is the $J$ integral evaluated around the crack tip (during cracking $J_{\text{tip}}$ is equal to the fracture energy of the tip, $J_0$). The integral is the energy dissipation in the bridging zone and $\delta^*$ is the end-opening of the bridging zone at the notch root.

Connection is made to the overall R-curves as follows. By definition $J_R$ is the value of $J$ during crack growth. Initially, the crack is unbridged. Thus, by Eq. (2), crack growth initiates when $J_R = J_{\text{tip}} = J_0$. As the crack grows, $J_R$ increases in accordance with Eq. (2). When the end opening of the bridging zone $\delta^*$ reaches $\delta_0$, the overall R-curve attains its steady state value $J_{ss}$.

The bridging law can be determined by differentiating Eq. (2) [4].

$$\sigma(\delta^*) = \frac{\partial J_R}{\partial \delta}$$ \hspace{1cm} (3)

The applied moment and the end opening of the bridging zone $\Delta u^*_2$ are recorded. Assuming that $\delta^* \approx \Delta u^*_2$, where $\Delta u^*_2$ is the notch opening measured at the neutral axis of the DCB specimen (see Figure 1), the bridging law can be determined. This approach models the bridging zone as a discrete mechanism on its own. Contrary to crack growth resistance curves (R-curves), the bridging law can be considered a material property and does not depend on specimen size [2].

The test above has been modified with two different bending moments to result in mixed mode testing [5]. In this case, mixed bridging laws can be measured. This has not been undertaken for the current material selection.

3 Experimental results

Recently, we have, by the use of a $J$ integral based approach, measured the bridging laws under mode I fracture during transverse splitting of unidirectional glass-fiber/epoxy and glass-fiber/polyester composites with different interface characteristics [3]. With increasing applied moment, crack propagation took place. Fibre cross-over bridging developed in the zone between the notch and the crack tip. $J_R$ is calculated according to Eq. (1). The specimen width $b$ was 5 mm with a beam height of $H=8$ mm. Assuming that the unidirectional composite is transversely isotropic, the following elastic composite data were applied for Eq. (1) as previously measured: $E_{11,\text{epoxy}}= 41.5$ GPa, $E_{33}= 9.2$ GPa, $E_{11,\text{polyester}} = 42$ GPa, $E_{33,\text{polyester}}= 10$ GPa and $\nu_{13}=0.3$ (assumption). The analytical function

$$J_R(\delta^*) = J_0 + \Delta J_{ss} \left( \frac{\delta^*}{\delta_0} \right)^\frac{2}{\hat{\nu}}$$ \hspace{1cm} (4)

was found to fit all experimental data curves of crack growth resistance versus crack opening well, resulting in curve fits as shown in Figure 2. $J_0$ is the initial value of the experimental curve and equal to the fracture energy of the tip during crack growth, while $\Delta J_{ss}$, which is equal to $(J_{ss} - J_0)$, is the increase in crack growth resistance. Sørensen and Jacobsen [2] found that the same function fit the data of carbon fibre composite systems well.

The experimental values for the bridging laws are given in Table 1. The starting value $J_0$ indicates the point of crack growth initiation and can easily be determined during the experiment. The highest value of 345 J/m$^2$ was observed for the sizing B/epoxy system. The crack
initiation value is significantly lower for the sizing B/ polyester system with 120 J/m$^2$, which is also related to a significantly lower transverse strength of not more than half the strength of the other composites [3]. In fact, the relation between transverse strength (measured with the transverse bending test [3]) and crack initiation for the different composite systems is fitted well by a linear relationship as seen in Figure 3. This verifies the assumption that the crack initiation value for the DCB test is controlled by the strength of the fiber-matrix bond. It can furthermore be seen that both axes show about the same ratio low strength and high strength of nearly a factor 3; however, the transverse bending test results in much lower standard deviations. The higher standard deviation for the DCB fracture initiation is most likely explained by specimen-to-specimen differences of the manufactured notch, which influences the crack initiation process.

The end opening value $\delta_0$ at the onset of steady-state cracking was determined to be 2 mm for the epoxy systems. For the sizing B/ polyester system, steady-state cracking could not be determined with the present specimens, as the fibres continued to bridge the whole length of the crack after the maximum measurable notch opening of 5 mm was obtained. Since no upper bound was found for $\Delta J_{ss}$, this bridging behaviour was termed ‘infinite toughening’.

Differentiating equation (4) results in the bridging law

$$\sigma(\delta) = \frac{\Delta J_{ss}}{2\delta_0} \left( \frac{\delta}{\delta_0} \right)^{-\frac{1}{2}}, \quad \text{for} \quad 0 < \delta < \delta_0,$$

where $\Delta J_{ss}$ is the increase in crack growth resistance due to bridging (from zero to steady state bridging), and $\delta_0$ is the crack opening where the bridging stress vanishes.

The bridging laws for the different fibre systems are compared in Figure 4. The bridging law can be considered a material property [2, 6] and is in an accessible form for implementation into finite element codes.
Figure 3. Relationship between crack initiation value $J_0$ and the transverse composite strength for the different composite systems

Figure 4. Comparison of resulting bridging laws

4 Implementation of the user element

There are a variety of possible methods for implementing cohesive laws within commercial finite element programs. The most versatile is the development and programming of cohesive elements [7–10]. These elements are in most cases defined with zero thickness and prescribe stresses based on the relative displacement of the nodes of the element. Similar work has also been undertaken with spring elements (force-opening relation), although in this case there might be simplifications required when calculating the equivalent nodal spring forces from the surrounding elements. The procedure is not straight forward when springs are connected to elements with non-linear shape functions, such as 8-noded elements [11].
4.1 Background and basic equations

Figure 5 shows the interface elements in 2-D and 3-D. The interface element is made up of two quadratic line elements (a), or two quadratic 2-D plane elements (b). These elements connect the faces of adjacent elements during the fracture process. The implementation is based on [10].

Quadratic elements are chosen as the cantilever beam mostly deforms under bending, which is best modelled by quadratic solid elements. The nodes of the interface element need to fit to these elements. The node numbering is chosen according to ABAQUS conventions of quadratic 2-D and 3D solids. The elements can also be derived in linear form by substituting the quadratic shape functions with linear ones in the appropriate equations. Consequently, the node numbers and degree of freedoms for the elements will decrease.

The two surfaces of the interface element initially lie together in the unstressed deformation state (zero thickness) and separate as the adjacent elements deform. The relative displacements of the element faces create normal and shear displacements, which in turn generate element stresses depending on the constitutive equations (stress - opening relations) of the material. The constitutive relationship was derived in the experimental section, and is independent of the element formulation.

The implementation of a general interface element is explained in the following. Details are given for the special case of the quadratic line element for 2-D simulations, while the equivalent formulations for the quadratic plane element for 3-D simulations are provided in Appendix A.

The line interface element has 12 (2x6) degrees of freedom. The vector \( \mathbf{d} \) of the nodal displacements in the global coordinate system is given as:

\[
\mathbf{d} = \begin{bmatrix} d_1^x & d_1^y & \cdots & d_6^x & d_6^y \end{bmatrix}^T
\]

The order follows typical ABAQUS’ conventions, and this is considered in the derived formulations below.

The opening of the interface element is defined as the difference in displacements between the top and bottom nodes:

\[
\Delta \mathbf{u} = \{ u \}_\text{top} - \{ u \}_\text{bot} ,
\]

thereby leading to the following definition of the interface opening \( \Delta \mathbf{u}_N \) in terms of nodal displacements of paired nodes:

\[
\Delta \mathbf{u}_N = \Phi \mathbf{d}_N = [-I_{6\times6} \mid \mathbf{I}_{6\times6}] \mathbf{d}_N
\]

where \( I_{6\times6} \) denotes a unity matrix with 6 rows and columns. \( \mathbf{u}_N \) is a \( 6 \times 1 \) vector.

From the nodal positions, the crack opening is interpolated to the integration points with the help of standard shape functions. Let \( N_i(\xi) \) be the shape functions for the node pair \( i \) \((i = 1, 2, 3)\), where \( \xi \) stands for the local coordinate of the element with \(-1 \leq \xi \leq 1\). The
relative displacement between the nodes for each point within the element is then given by:

$$\Delta u(\xi) = \begin{pmatrix} \Delta u_x(\xi) \\ \Delta u_y(\xi) \end{pmatrix} = \mathbf{H}(\xi)\Delta \mathbf{u}_N,$$

(9)

where \(\mathbf{H}(\xi)\) is a 2x6 matrix containing the quadratic shape functions.

For the line element, it is of the form

$$\mathbf{H}(\xi) = \begin{pmatrix} N_1(\xi) & 0 & N_2(\xi) & 0 & N_3(\xi) & 0 \\
0 & N_1(\xi) & 0 & N_2(\xi) & 0 & N_3(\xi) \end{pmatrix}.$$  

(10)

The shape functions for the line element are given in Appendix B. As a result, we get

$$\Delta u(\xi) = \mathbf{H}(\xi)\Phi \mathbf{d}_N = \mathbf{B}(\xi) \mathbf{d}_N,$$

(11)

where \(\mathbf{B}(\xi)\) is of the dimension 2x12 and \(\Delta u(\xi)\) of the dimension 2x1; thereby describing the continuous displacement field in both directions within the element.

During large deformations, the element requires a local coordinate system to compute local deformations in normal and tangential directions. The most common choice is a coordinate system given by the middle points of the two element faces, which thereby coincides with the nodal positions in the undeformed state. If the coordinates of the initial configuration are given by the vector \(\mathbf{x}_N\) and the deformation state is defined by the vector \(\mathbf{d}_N\), the reference surface coordinates \(\mathbf{x}_R\) are computed by linear interpolation between the top and bottom nodes in their deformed state:

$$\mathbf{x}_R = \frac{1}{2}(\mathbf{I}_{6 \times 6} | \mathbf{I}_{6 \times 6})(\mathbf{x}_N + \mathbf{d}_N)$$

(12)

The coordinates of any specific reference plane can be derived analogous to Eq. (9):

$$\mathbf{x}_R(\xi) = \begin{pmatrix} x_R(\xi) \\ y_R(\xi) \end{pmatrix} = \mathbf{H}(\xi)\mathbf{x}_N$$

(13)

This local coordinate vector, with unit length, is obtained by differentiating the global position vector with respect to the local coordinates:

$$\mathbf{t}_1 = \frac{1}{||\frac{\partial \mathbf{x}_R}{\partial \xi}||} \left( \frac{\partial x_R}{\partial \xi}, \frac{\partial y_R}{\partial \xi} \right)^T.$$  

(14)

The normal vector (also with unit length) of the local coordinate element needs to be perpendicular to the vector \(\mathbf{t}_1\):

$$\mathbf{t}_n = \frac{1}{||\frac{\partial \mathbf{x}_R}{\partial \xi}||} \left( -\frac{\partial y_R}{\partial \xi}, \frac{\partial x_R}{\partial \xi} \right)^T,$$

(15)

and the derivatives are determined as follows:

$$\frac{\delta \mathbf{x}_R(\xi)}{\delta \xi} = \begin{pmatrix} x_R(\xi) \\ y_R(\xi) \end{pmatrix} = \frac{\delta (\mathbf{H}(\xi))}{\delta \xi} \mathbf{x}_N = \mathbf{h}(\xi)\mathbf{x}_N,$$

(16)

with

$$\mathbf{h}(\xi) = \begin{pmatrix} N_1,\xi(\xi) & 0 & N_2,\xi(\xi) & 0 & N_3,\xi(\xi) & 0 \\
0 & N_1,\xi(\xi) & 0 & N_2,\xi(\xi) & 0 & N_3,\xi(\xi) \end{pmatrix}.$$  

(17)

The derivatives of the shape functions are given in Appendix B.

The length of the vector is given by the standard definition:

$$||\frac{\partial \mathbf{x}_R}{\partial \xi}|| = \sqrt{\left( \frac{\partial x_R}{\partial \xi} \right)^2 + \left( \frac{\partial y_R}{\partial \xi} \right)^2},$$

(18)

The components \(\mathbf{t}_1\) and \(\mathbf{t}_n\) represent the direction cosines of the local coordinate system to the global one, thus defining the 2x2 transformation tensor \(\Theta\):

$$\Theta = [\mathbf{t}_1, \mathbf{t}_n],$$

(19)
which relates the local and global displacements as follows:

$$\Delta u_{\text{loc}} = \Theta^T \Delta u. \quad (20)$$

In the following, local matrices will be indicated by the subscript $\text{loc}$ as above, while the equations otherwise refer to the global values.

$t_{\text{loc}}$ is the $1 \times 2$ vector defining the bridging stresses in the local coordinate system and relates to the local relative displacement via the constitutive expression for the interface element:

$$t_{\text{loc}} = \begin{pmatrix} \sigma_1 \\ \sigma_n \end{pmatrix} = C_{\text{loc}} (\Delta u_{\text{loc}}) \Delta u_{\text{loc}} \quad (21)$$

The constitutive expression can be expressed either with a linear displacement term for $\Delta u$ as shown above, or with a coupled form, where $\Delta u$ is included with non-linear dependence. The preferred option depends on the form of the constitutive equation. For our expression as introduced in Eq. (5), a coupled form is preferable:

$$t_{\text{loc}} = C_{\text{loc}} \Delta u_{\text{loc}}^{-2} \quad (22)$$

$C_{\text{loc}}$ is now a constant, and does not depend on the displacement. Note that in comparison with Eq. (5), $\delta$ has been replaced with the general numerical nomenclature for the opening, $\Delta u_{\text{loc}}$. This convention will be kept in the following.

The element stiffness matrix and the right hand side nodal force vector are required for the UEL subroutine in ABAQUS.

The element force vector is of size $12 \times 1$. Its contribution to the global force vector is defined as

$$f_N^e = \int_{A_e} B^T t \, dA \quad (23)$$

$$= W \int_{L_e} B^T t \, dl \quad (24)$$

$$= W \int_{-1}^{1} B^T t \, \det J \, d\xi \quad (25)$$

$$= W \int_{-1}^{1} B^T \Theta_{\text{loc}} \, \det J \, d\xi, \quad (26)$$

where $W$ is the width of the interface element and, as in most cases of 2-D modelling, also the width of the finite element model.

$\det J$ is the Jacobian defined by the transformation of the global coordinates $(x, y)$ to the current element coordinate $(\xi)$, and results, for the line element, in the same expression as previously used for calculating the length of the unit vector in Eq. (18):

$$\det J = \sqrt{\left(\frac{\partial x}{\partial \xi}\right)^2 + \left(\frac{\partial y}{\partial \xi}\right)^2}. \quad (27)$$

It should be noted that the Jacobian will in most analysis cases not be constant, but depend on the local element coordinates. It therefore needs to be derived for each integration point.

The tangent stiffness matrix of according size $12 \times 12$ (note negative sign convention for ABAQUS) is defined as

$$K^e = \frac{\partial f_N^e}{\partial \mathbf{d}_N} \quad (28)$$
With the derivation from Eq. (23), this results in:

\[ K = - \int_{A_d} B^T \Theta \frac{\partial \delta_{\text{loc}}}{\partial \delta_{\text{det}}} \, dA \]  
\[ = - W \int_{L_d} B^T \Theta \frac{\partial \delta_{\text{loc}}}{\partial \delta_{\text{det}}} \, dl \]  
\[ = - W \int_{L_d} B^T \Theta \frac{\partial \delta_{\text{loc}}}{\partial \delta_{\text{det}}} \frac{\partial \Delta u_{\text{loc}}}{\partial \Delta u} \frac{\partial \Delta u}{\partial \delta_{\text{det}}} \, dl \]  
\[ = - W \int_{L_d} B^T \Theta \frac{\partial \delta_{\text{loc}}}{\partial \delta_{\text{det}}} \Theta^T B \, dl \]  
\[ = - W \int_{-1}^{1} B^T \Theta \delta_{\text{loc}} \Theta^T B \, \det J \, d\xi \]  

As can be seen above, the stiffness matrix \( D \) is defined as:

\[ D_{\text{loc}} = \frac{\partial \delta_{\text{loc}}}{\partial \Delta u_{\text{loc}}} \]  

It can also be expressed in terms of the constitutive expression in Eq. (21) with a linear dependence on \( \Delta u \):

\[ D_{\text{loc}} = \frac{\partial C(\Delta u)\Delta u}{\partial \Delta u} = \frac{\partial C(\Delta u)}{\partial \Delta u} \Delta u + C(\Delta u) \]  

The local traction matrix \( D \) is then given by

\[ D_{\text{loc}} = \begin{pmatrix} D_1 & \Sigma \end{pmatrix} \begin{pmatrix} D_c & D_n \end{pmatrix} \]  

The terms \( D_1 \) and \( D_n \) are derived by finding the derivatives according to Eq. (34). The components \( D_c \) are possible coupling terms. They are normally obtained if the traction laws are derived from an overall elastic potential for the cohesive law. For mode I loading, the relative displacement in \( u_1 \) direction will be zero, and thereby will lead to zero traction stresses. The coupling terms can then be set to zero as they do not influence the results. However, a dummy value for \( D_1 \) should be assigned to avoid possible numerical problems due to a singular stiffness matrix.

### 4.2 Unloading

The cohesive relationship as given in Eq. (21) is elastic, and the stresses transferred through the crack obey the same law whether the crack opens or closes. For most damage mechanisms, this is most likely not true, as it neglects the damage introduced at the interface during partial opening. Local unloading can occur when sudden changes occur in the external loading application or internal stress redistribution in neighbouring elements is introduced, for example by sudden failure. This problem is usually circumvented by introducing a maximum damage parameter \( \Delta u^{*} \), which stands for the maximum value the opening \( \Delta u \) obtained during a given increment of the analysis. If the next value of \( \Delta u \) is larger, damage continues to grow; otherwise elastic unloading is assumed to occur (see the elastic unloading towards the origin in the simplified bridging law in Figure 6). This elastic mechanism applies for the case of fibre bridging. The failure mechanism includes peeling off of the fibers on either side of the crack, the development of bridging fiber ligaments and failure of these ligaments. During unloading, however, these ligaments simply deform elastically, thereby leading to a elastic unloading towards the origin, which can be observed during the experiment. For numerical purposes it should be noted that \( \Delta u^{*} \) should only be updated at the end of the increment; the current iterative opening solution is not considered.
4.3 State variables for the analysis

The number of state variables for each element depends on the chosen integration procedure. At each integration point of the linear element, there are two state variables for the relative displacement (x- and y-direction) and the local stresses acting in the x- and y-direction. Furthermore, there is one value describing the current interaction state between the two surfaces for the purpose of stability analysis and position tracking during the numerical procedure. For a standard integration procedure with 3 integration points, this results in 15 (3x5) state variables for the linear element. As ABAQUS will not terminate the analysis if an insufficient number of state variables is provided for the chosen integration procedure, this user input error is checked within the UEL subroutine.

4.4 Numerical integration

The integration scheme was previously shown to have an influence on the performance of the interface element. For the Newton-Cotes scheme, the integration points are located at the nodes. For linear elements, the application of the standard Gauss integration was shown to result in a coupling between the degrees of freedom of different node sets and then in oscillations of the traction profile in the presence of large traction gradients, such as during the initial stiffness increase prior to damage, over the element [7].

Non-convergence can occur if the element size is too large compared to the stress uptake based on the traction law: around three elements should be present to resolve the changing stress state in the interface [12]. This restriction on maximum element size can also be circumvented by choosing a larger number of integration points instead of reducing the element size [13]. These approaches are investigated in the report. The 3 point rules (normally chosen as standard in accordance with the quadratic element type) as well as 6 and 12 point rules for Gauss and Newton-Cotes were tested with the current model. The respective points and weights are given in Appendix C.
5 Numerical model

The beam-contact problem is shown in Figure 7. Owing to symmetry, it is sufficient to consider only one beam of the DCB specimen, although full models were run to check the boundary conditions (see Section 5.1). The thickness \( H \) is equal to 8 mm, as discussed in Section 3. Plane strain conditions were assumed, which neglect edge effects. The mesh consisted of eight-noded plane strain solid CPE8 elements, which are suited to describe bending deformations without hourglass effects. The composite material is assumed to be transversely isotropic. The material properties were given in the previous section. Nonlinear, large displacements are considered in the analysis to rotate the non-isotropic material properties accordingly. The interface elements are applied from the crack tip onwards to the end of the beam. The lower element nodes are fixed on the symmetry line.

5.1 Symmetric half model versus full model: boundary conditions

For the symmetric half model, additional nodes are duplicated on the symmetry line for the description of the user elements. During deformation, the top and bottom node sets will separate. The bottom nodes are therefore fixed in the y-direction as a zero boundary condition. However, preliminary analyses showed that this condition does not truly represent the full model (see Figure 8), as the nodes can move freely along the symmetry line in the x-direction. For the true full model, the bottom nodes will move in the same way as the top nodes, therefore maintaining the same absolute x-value (no shear introduced in the elements). As discussed previously, the coupling terms in the local constitutive equation (see Eq. (36)) therefore do not influence the results. For the symmetric half model, the movement of the symmetry nodes in x-direction therefore needs to be coupled to the movement of the beam nodes via *EQUATION for each duplicated node. The difference in deformation is visualised in Figure 8. The effect is rather small, but leads to differences in the results for larger beam deflection.

5.2 Application of pure moment bending by displacement control

In analyses with possible decreasing stresses due to introduced material damage or decreasing bridging stresses, displacement controlled deformation, rather than force controlled, becomes the preferred way of introducing boundary conditions. Furthermore, displacement control is mostly applied in experimental testing and also in the case of the DCB specimen testing: testing procedure and simulation are therefore more closely related.

The application of a homogeneous bending moment by force control requires consideration
of the element type, where varying forces need to be applied to each node according to the underlying shape functions of the element type. This procedure does unfortunately not work for displacements, as for a given translation the rotation of the beam is unknown. The displacement controlled procedure introduced in the following was first described in previous work of the group [11]. The moment application is simplified, thereby resulting in a non-homogeneous displacement field towards the nodes of the applied boundary conditions at the end of the beam. However, pure bending conditions are introduced around the contact zone by ensuring that the distance \( k \) between moment introduction and beginning of the contact zone (see Figure 7) is large enough. The value is set to \( k=12 \) mm in the present model, with \( s \) being equal to 24.65 mm. As a rule of thumb, boundary conditions should be applied more than the beam thickness away from the point of interest to ensure a uniform bending stress in this region. With a beam height of \( H=8 \) mm, this requirement is fulfilled.

The displacements \( v_1 \) and \( v_2 \) of two nodes are to be controlled such that the resulting forces in the two nodes, \( F_1 \) and \( F_2 \), are equal and opposite in magnitude, thereby introducing a pure bending moment (see Figure 9). To accomplish this, \( v_1 \) and \( v_2 \) are inter-connected by two so-called dummy nodes A and B, having displacement \( v_A \) and \( v_B \), respectively. Dummy nodes are nodes that are not associated with the geometry. Their positions with respect to the beam structure are arbitrary, but their displacements have to be coupled to the structural deformation. In the following we derive a suitable relationship between \( v_1 \), \( v_2 \), \( v_A \) and \( v_B \). The displacement of node A is defined as

\[
v_A = v_1 - v_2.
\]
Note that $\theta = \arctan(v_A/s) \approx v_A/s$ is the rotation of the beam, with $s$ being the current difference in $x$-coordinates between nodes 1 and 2, i.e. the moment arm. The distance between the nodes will change during the analysis, which needs to be taken into account. The displacement in node $B$ is set to
\[ v_B = v_1 + v_2. \] (38)
These definitions are mostly arbitrary. In a physical sense, they can be described as a rotational component (displacement $v_A$) and a translational component (displacement $v_B$). We have to make sure that the same energy is applied to the dummy nodes as is applied to the structure itself. Using the principle of virtual work, the applied incremental elastic energy $dw$ is given by
\[ dw = F_1 dv_1 + F_2 dv_2. \] (39)
Similarly, the incremental elastic energy applied to nodes $A$ and $B$ is
\[ dw = F_A dv_A + F_B dv_B, \] (40)
where $F_A$ and $F_B$ are the forces in nodes $A$ and $B$. Finally, the pure bending condition $F_1 + F_2 = 0$ (41) must hold true. As the forces in the dummy nodes must (according to the principle of virtual work) perform the same incremental work as $F_1$ and $F_2$, the following relations are obtained:
\[ dw = F_A d(v_1 - v_2) + F_B d(v_1 + v_2) \] (42)
\[ = (F_A + F_B)dv_1 + (F_B - F_A)dv_2 \] (43)
In comparison with Eq. (39), this gives the correct relationships between the forces at the nodes.
\[ F_1 = F_A + F_B \quad \text{and} \quad F_2 = F_B - F_A \] (44)
\[ 2F_A = F_1 - F_2 \quad \text{and} \quad 2F_B = F_1 + F_2 \] (45)
Comparing Eq. (41) and Eq. (44) leads to $F_B = 0$, and gives $F_A = F_1$. Thus, the constraints between $v_1$, $v_2$, $v_A$ and $v_B$ in Eq. (37) and Eq. (38) provide the correct constraints. The multi-point constraints for the numerical analysis are implemented into the finite element analysis as follows:
\[ 2v_1 - v_A - v_B = 0 \quad \text{and} \quad 2v_2 + v_A - v_B = 0 \] (46)
In summary, $F_A$ is the applied force to the beam from which the moment $M = F_As = F_1s$ can be calculated for each increment. $J_R$ is computed from Eq. (1) using the calculated moment. The displacement $v_A$ is the loading parameter, which is increased during the simulations.
5.3 Bridging law adjustments for numerical modelling

The bridging law adjustment is shown exemplary for the case of the normal stresses across the interface element. With the experimental result of Eq. (5), the stress $\sigma_n$ in normal direction was defined by the following non-linear relationship

$$\sigma_n = K_n \Delta u_{n}^{-\frac{1}{2}} \quad \text{with} \quad C_n = \frac{\Delta J_{ss}}{2 \sqrt{\Delta u_{0,n}}} \quad \text{for} \quad 0 < \Delta u_n < \Delta u_{0,n}. \quad (47)$$

$C_n$ is a constant in this case as explained in Eq. (22). In the following, we will drop the subscript $n$ for the normal direction. The relationship above can easily be modified or be expressed in piecewise linear form for different values of $\Delta u$. The use of numerical predictions for a better fit of the cohesive law to the experimental results will be discussed later.

Two points need to be addressed during the numerical adjustment:

- Removal of the stress singularity at $\Delta u = 0$
- Incorporation of the initial fracture strength $J_0$.

The bridging law as shown in Eq. (47) has a simple form, but inhibits a singularity in $\Delta u = 0 : \sigma \to \infty$ (see Eq. 5). Two different methods of numerical adjustment are thought of in the following as shown in Figure 10. Micromechanical considerations [6] predict that crack initiation starts at a finite stress value at the interface, which is reached as the deformation starts to take place. At the point of crack initiation, the opening at the interface will still be zero (a). This option is unconventional in the numerical sense; and the implications of using a non-zero starting value for the analysis results have been explored previously [14]. In the case of a finite stress value $\sigma_0$ for $\Delta u = 0$ in the bridging law, the finite element model actually starts with a force imbalance, as no initial force equilibrium is given. This is resolved in the first increment by overlapping the user elements to achieve zero stresses away from the crack tip. From a numerical point of force equilibrium at analysis start, a zero stress state is preferable at $\Delta u = 0$ as shown in (b). For small values of $\Delta u_1$ and $\Delta u_2$ these adjustments are only have a small influence on the numerical results.

![Figure 10. Possibilities of circumventing infinite stress at zero opening](image)

For both methods, the most important point is that the energy uptake is equivalent in the experimental and numerical model. Details of the bridging law can then be adjusted in the numerical evaluation to provide the best fit with the experimental data.

The differentiation of Eq. (4), which leads to Eq. (5), results in a loss of the initial starting value $J_0$ (see Table 1). It is therefore not advisable to simply use the earlier bridging law
parameters for the numerical simulations. A further adjustment is required. Two procedures based on the initial choice of singularity treatment above are proposed.

The total energy uptake of the experimental bridging law is determined by calculating the area $W_{\text{exp}}$ under the curve.

$$W_{\text{exp}} = J_{\text{ss}} + J_0$$ for $\Delta u \in [0, \Delta u_0]$  \hspace{1cm} (49)

For a better physical understanding, $J_0$ can be considered as the elastic fracture energy during initial fracture (strength controlled), while $J_{\text{ss}}$ represents the damage fracture energy (toughness controlled). To ensure the same energy uptake during the simulated fracture process, the initial starting value $J_0$ needs to be incorporated into the bridging law.

For the two bridging law adjustments introduced in Figure 10, different ideas for including $J_0$ are suggested. For method (a) with a finite stress, the initial linear bridging law part can be further adjusted in such a way that the numerical fracture energy at the point $\Delta u_1$ of the bridging process also includes the experimental starting value $J_0$, while for method (b), the easiest method is to split the two components $J_0$ and $J_{\text{ss}}$ within the bridging law, and use the initial increase up to $\Delta u_2$ to introduce $J_0$. In the following, the necessary adjustments are derived for both cases, and the results are compared with the experimental data.

**Finite stress value**

Figure 11 visualises method (a). Upon reaching the value of $\Delta u_1$, the energy uptake is adjusted according to the experimental curve:

$$W_{\text{exp}} = \Delta J_{\text{ss}} \left( \frac{\Delta u_1}{\Delta u_0} \right)^{\frac{1}{2}} + J_0$$ for $\Delta u \in [0, \Delta u_1]$  \hspace{1cm} (50)

The maximum stress $\sigma_0$ is found by requiring that the area $W_1$ of the linear softening law in the range $0 \leq \Delta u \leq \Delta u_1$ must equal the area $W_{\text{exp}}$ of the general bridging law in this range (see Eq. (50)). Area $W_1$ is given by

$$W_1 = \frac{\Delta J_{\text{ss}}}{2} \left( \frac{\Delta u_1}{\Delta u_0} \right)^{\frac{1}{2}} + \frac{1}{2} \sigma_0 \Delta u_1$$ \hspace{1cm} (51)
Setting $W_{\text{exp}} = W_1$, while including the starting value $J_0$ as before, the maximum stress for a given $\Delta u_1$ (as also included in Figure 11) is

$$\sigma_0 = \frac{2J_0 + \frac{4}{3} \Delta J_u \left( \frac{\Delta u}{J_0} \right)^{\frac{3}{2}}}{\Delta u_1}$$  \hspace{1cm} (52)

The energy uptake $J_0$ is now included by raising the starting value $\sigma_0$. Upon reaching of $\Delta u_1$, the original bridging law is followed.

**Zero stress value**

In the second case of a zero stress condition with subsequent increase of the bridging stresses, a separated bridging law is proposed. A small value of $\Delta u_2$ is assumed, up to which the energy $J_0$ is taken up. This is the physically more relevant case, and is visualised in Figure 12.

![Figure 12. Numerical bridging law (adjusted). Separated fracture energy.](image)

The value of $\Delta u_2$ is determined from the initial fracture energy of $J_0$ and the maximum stress value, $\sigma_0$. $\Delta u_2$ can become quite small based on the value of $J_0$. This leads to convergence problems with the linear increase shown in Figure 12(a), as a steep tangent changes to a negative tangent at $\Delta u = \Delta u_2$. Instead of the common linear increase up to $\Delta u_2$, a power-law approach (see Figure 12(b)) can be applied:

$$\sigma(\Delta u) = \sigma_0 \left( 1 - \left( \frac{\Delta u_2 - \Delta u}{\Delta u_2} \right)^{\alpha} \right)$$  \hspace{1cm} (53)

$\Delta u_2$ can then be determined for a given value of $J_0$ by the integral:

$$\int_0^{\Delta u_2} \sigma(\Delta u) d\Delta u = J_0$$  \hspace{1cm} (54)

$$\Rightarrow \Delta u_2 = \frac{J_0}{\sigma_0} \left( \frac{\alpha + 1}{\alpha} \right)$$  \hspace{1cm} (55)

The power-law factor $\alpha$ determines the initial increase in stress with opening. The increase with $\alpha = 1$ is identical to the linear increase. A factor of $\alpha = 100$ leads to the same result for $\Delta u_2$ as a constant finite stress value up to $\Delta u_2$ with a non-equilibrium starting point as shown in Table 2. The proposed power-law function is therefore more versatile. However, the power-law approach is also a numerically more robust approach as the sign change in the tangent stiffness
Table 2. Comparison of values for $\Delta u_2$ for a maximum stress of $\sigma_0 = 3.5$ MPa and $J_0 = 300$ J/m$^2$

<table>
<thead>
<tr>
<th>$\alpha$</th>
<th>$\Delta u_2$ [mm]</th>
<th>$\alpha=1$</th>
<th>$\alpha=5$</th>
<th>$\alpha=10$</th>
<th>$\alpha=100$</th>
<th>linear</th>
<th>const $\sigma_0$</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>0.1714</td>
<td>0.1029</td>
<td>0.0943</td>
<td>0.0857</td>
<td>0.1714</td>
<td>0.0857</td>
<td></td>
</tr>
</tbody>
</table>

at $\Delta u = \Delta u_2$ is avoided. The tangent is close to zero before becoming negative. This leads to largely improved numerical behaviour in terms of convergence.

As the energy uptake for the damage part needs to equal the original $\Delta J_{ss}$ for whole bridging law between 0 and $\Delta u_0$, the value of $\Delta J_{ss, \text{adj}}$ (see Figure 12) in the bridging law needs to be adjusted accordingly:

$$
\Delta J_{ss, \text{adj}} = \frac{\Delta J_{ss}}{1 - \sqrt{\frac{\Delta u_2}{\Delta u_0}}}
$$

(56)

The problem with this procedure is that $\sigma_0$ in turn again depends on the adjusted value for $\Delta J_{ss, \text{adj}}$:

$$
\sigma_0 = \frac{\Delta J_{ss, \text{adj}}}{2\sqrt{\Delta u_2 \Delta u_0}}
$$

(57)

As a consequence, $\sigma_0$ and therefore $\Delta u_2$ will change as well. An iterative procedure needs to be applied to determine the values for $\Delta J_{ss, \text{adj}}$ and $\Delta u_2$. A starting value for $\Delta u_2$ in Eq. (56) can be determined by integrating the bridging law within the limits of $\Delta u_2$ and $\Delta u_0$ and setting the integral value equal to $\Delta J_{ss}$ (see Figure 12 (b)):

$$
\Delta u_2 = \frac{4J_0^2}{\Delta J_{ss}^2} \left( \frac{\alpha + 1}{\alpha} \right)^2
$$

(58)

We therefore determine first $\Delta u_2$ with Eq. (58). This is followed by calculating $\Delta J_{ss, \text{adj}}$ with Eq. (56) and $\sigma_0$ is calculated with Eq. (57). In the last step, $\Delta u_2$ is determined with Eq. (55). The latter three steps are repeated until convergence is achieved.

6 Numerical results

In the following sections, results are introduced for the bridging law shape with finite stress value (method (a)) and the bridging law shape with split values of $J_0$ and $\Delta J_{ss}$ with a power-law increase (method (b)) as introduced earlier. Based on these results, the bridging law shape was then modified to give a better fit with the experimental data. To justify this procedure, it is important to keep in mind that the bridging law was derived in the first place by analytical curve fitting of the crack growth resistance (see Section 2). Any deviation from the experimental data to this curve fitting will of course influence the match of the predictions. Analytically, it is only possible to fit the crack growth resistance. In this section, we also compare the numerical fit with other experimental observations, such as the crack growth resistance as a function of crack growth development and the shape of the crack opening. Based on these findings, it is shown that the crack growth resistance as a function of crack growth development is much more sensitive to changes in the bridging law shape. Numerical adjustment of the bridging law parameters on this curve leads to a very good agreement of the numerical predictions with all measured experimental data.

6.1 Comparison of numerical integration procedures

Non-convergence problems can occur in the following simulations if the element size is too large compared to the stress uptake based on the traction law. This is most severe for the method of the finite stress value, while all approaches with the power-law show a better numerical
performance. This restriction on element size can be improved by choosing a larger number of integration points instead of reducing the element size.

Different mesh refinements were used to determine the numerical response during the simulations. For the first refinement, which was shown in Figure 8, the element length for the cohesive elements is given by 0.36 mm. This is in relation to values of $\Delta u_1 = 0.05 \text{ mm}$ and $\Delta u_2 \approx 0.01 \text{ mm}$. The mesh density is not sufficient to ensure convergence for 3 Gauss integration points and Newton-Cotes points for all methods introduced in the following: further mesh refinement for those integration procedures is required down to $l_{el} = 0.0906 \text{ mm}$ is required. At this level, all integration procedures converge.

However, the use of 6 integration points for both the Gauss and Newton-Cotes integration result in convergence of the simulation. Running the simulation with a lower mesh density is of course beneficial in terms of computational effort. For 12 integration points, Gauss integration again shows good performance, while the 12 point Newton-Cotes integration does not converge after an initial start of the analysis. Some oscillatory effects in the numerical predictions of the steady-state level are observed for a large number of integration points and low mesh density.

6.2 Numerical bridging results with finite stress value

Calculations were undertaken by method (a) with a finite stress value. The value of $J_0$ was included as described earlier. Two experimental tests were simulated: sizing A / epoxy and sizing A / polyester. As specific experiments were modelled, the data was not fitted to the average of all tests as shown in Table 1, but to the specific test data of some of the specimens (one for sizing A / epoxy, two for sizing A / polyester). For these specimens, we also recorded the crack length as a function of crack growth resistance.

The following data were used for the bridging law (for reference for the numerical nomenclature see Figure 11):

Sizing A / epoxy:
- $\Delta J_{ss} = 3300 \text{ J/m}^2$
- $J_0 = 300 \text{ J/m}^2$
- $\Delta u_0 = 2 \text{ mm}$
- $\Delta u_1 = 0.05 \text{ mm} \rightarrow \sigma_0 = 27.65 \text{ MPa} (\sigma_1 = 5.22 \text{ MPa})$

Sizing A / polyester:
- $\Delta J_{ss} = 4000 \text{ J/m}^2$
- $J_0 = 130 \text{ J/m}^2$
- $\Delta u_0 = 5.5 \text{ mm}$
- $\Delta u_1 = 0.05 \text{ mm} \rightarrow \sigma_0 = 16.64 \text{ MPa} (\sigma_1 = 3.81 \text{ MPa})$

The value for $\Delta u_1$ has to be chosen as small as possible if we want to imitate the original bridging law shape. However, there are numerical limitations, as with decreasing $\Delta u_1$ the value of $\sigma_0$ increases rapidly. The current value of $\Delta u_1 = 0.05 \text{ mm}$ was found to lead to converging numerical studies with suitable levels of mesh refinement.

The fit with the crack growth resistance as shown in Figure 13 is satisfactory apart from slight differences in the initial increase of the crack growth resistance for small openings. For both materials, the model predicts a steeper increase than experimentally observed.

Figure 14 shows the comparison of the numerical predictions and the measured crack length for different values of crack growth resistance. The current numerical crack length is determined from a minimum positive node opening at the crack tip. An small threshold value of $1e-4$ is used to determine the crack tip position. The results are quite insensitive to threshold value variations between $1e-4$ and $1e-8$.

It can be clearly seen that this comparison is much more sensitive to the bridging law shape, and the figure shows the deficiency of the current bridging law shape clearly. Numerically, for
a given crack length a higher crack growth resistance is predicted.

The second concern is the initial prediction of crack growth. Naturally, due to the adjustment of the bridging law with respect to the incorporation of $J_0$ into the initial linear part, crack growth starts at zero crack growth resistance, and reaches the value after the initial crack opening of $\Delta u_1 = 0.05$ mm. This corresponds to approximate crack lengths of 5 mm for epoxy, and 3 mm for the polyester as indicated by the "knee" in Figure 14.

### 6.3 Numerical bridging results with power-law increase

We proposed splitting the values of $J_0$ and $\Delta J_{ss}$ with this method as shown in Figure 12. Using an iterative procedure with Eq. (56), (57) and (55) as explained previously, we get the following input values for the bridging law:

**Sizing A / epoxy:**

- $\Delta u_0 = 2$ mm
- $\alpha = 100$
\[ \Delta J_{ss} = 3300 \text{ J/m}^2 \]
\[ J_0 = 300 \text{ J/m}^2 \rightarrow \Delta u_2 = 0.048 \text{ mm, } \sigma_0 = 6.29 \text{ MPa and } \Delta J_{ss, adj} = 3906 \text{ J/m}^2 \]

Sizing A / polyester:
\[ \Delta u_0 = 5.5 \text{ mm} \]
\[ \alpha = 100 \]
\[ \Delta J_{ss} = 4000 \text{ J/m}^2 \]
\[ J_0 = 130 \text{ J/m}^2 \rightarrow \Delta u_2 = 0.021 \text{ mm, } \sigma_0 = 6.29 \text{ MPa and } \Delta J_{ss, adj} = 4262 \text{ J/m}^2 \]

The determined values for \( \Delta u_2 \) are small in comparison with \( \Delta u_0 \) \( (\frac{\Delta u_0}{\Delta u_2} > 20) \). It was therefore decided to keep \( \Delta u_0 \) constant.

Figure 15 shows the comparison between numerical and experimental data for the crack growth resistance as a function of crack opening. It can be seen that the numerical predictions for small crack openings fit better with the splitted damage energies than it did with method (a).

![Figure 15. Comparison of crack growth resistance for method (b) with power-law increase](image)

However, although the fit in Figure 15 seems nearly perfect with the proposed bridging law, the crack length as a function of the crack opening still shows deviations between the numerical and experimental results (see Figure 16). The current numerical crack length is determined from a minimum positive node opening at the crack tip, which is now defined as the value of \( \Delta u_2 \), and thereby corresponds to crack development after reaching the crack initiation energy of \( J_0 \).

In the opinion of the author, this assumption has a physically more valid background than a randomly chosen small threshold value: one can consider damage models where fracture is caused by initial microvoids forming and growing together [15]. Upon reaching the value of damage initiation \( J_0 \), these microvoids will form a starting crack.

### 6.4 Adjustment of bridging law

The above examples showed that the bridging law shape can be further improved to provide a better fit with the experimental data; especially with respect to the crack length as a function of crack growth resistance. Previous trials with the above models were taken as the basis to improve the fit. Based on the above results, the following conclusions are drawn:

- Splitting of the bridging law into an initial fracture \( (J_0) \) and damage \( (\Delta J_{ss}) \) controlled part is of advantage based on the physical interpretation of the cohesive law parameters. For
example, for a failure mode such as delamination between carbon / epoxy prepreg plies, the initial fracture energy surpasses the damage energy; while the bridging phenomenon of unidirectional glass fiber composites demonstrates the opposite material behaviour.

- Bridging law values should be derived from the experimental curves instead of being subject to a wide-ranging trial-and-error exercise. It is therefore desirable to estimate the value of $\Delta u_2$ from the experimental data directly. This would be even more important if $\Delta u_2$ is not small compared to $\Delta u_0$, i.e. both $J_0$ and $J_{ss}$ have comparable order of magnitude.

- For method (b) splitting of initial fracture ($J_0$) and damage ($J_{ss}$) fracture energy improves the fit with the experimental data. However, the initial drop in the bridging law (for values larger than $\Delta u_2$) is still too severe; thereby resulting in an overprediction of the crack growth resistance for a certain crack length.

This changes to the bridging law shape are visualised in Figure 17. The shape is basically a combination of the previously discussed shapes for method (a) and (b). For $K_{fac}=1$ and $\Delta u_1=0$ we reset the bridging law to the shape of method (b). The derivation of these values is explained below.

**Determination of $\Delta u_2$**

Previously, the value for $\Delta u_2$ was derived with a nonlinear solving procedure. However, as the value actually has a physical meaning as explained in the last section, the experimental curves can be used to determine suitable values. This is shown in Figure 18.

The figure shows that prior to crack initiation, the experimental curves actually exhibit a small displacement of the order of 10-20 µm. Upon reaching $J_0$, these microvoids will start to form the starting crack. The corresponding crack growth resistance is the value of $J_0$ as used previously ($J_{0,\text{epoxy}}=300$ J/m$^2$, $J_{0,\text{poly}}=130$ J/m$^2$).

The corresponding displacement value can be used for $\Delta u_2$. Based on Eq. (55), we can then calculate a value for the required peak stress and thereby introduce a factor value $K_{fac}$ in comparison to the original peak stress:

$$K_{fac} = \frac{J_0}{\Delta u_2 \sigma_0} \left( \frac{\alpha + 1}{\alpha} \right) \quad (59)$$

**Determination of $\Delta u_1$**

To improve the fit with the experimental data further, the decreasing bridging law slope after...
reaching $\Delta u_2$ needs to be reduced. We therefore take over the previously introduced linear decrease between $\Delta u_2$ and $\Delta u_1$ in this model.

This choice of an initially linear decrease is also based on previous numerical parameter studies of bridging law shapes [4, 11]. Bridging laws with linear decrease then show an s-shaped trend in the crack length development. Judging from the experimental data, where we overpredict more in the initial crack development stages, this type of model would improve the fit with the experimental data.

The value of $\Delta u_1$ now becomes the only adjustable value in the bridging law to be fitted to the experimental data points.

**Determination of $\sigma_0$**

$\sigma_0$ can be calculated from the requirement of total energy update equal to $\Delta J_{ss}$ as previously with model (a), without including $J_0$ as this part of the fracture energy is taken care of during
the initial increase up to $\Delta u_2$.

$$
\sigma_0 = \frac{\frac{3}{2} \Delta J_{ss} \left( \frac{\Delta u_1}{K_{fac}} \right)^{\frac{1}{2}}}{\Delta u_1}
$$

(60)

**Bridging law parameters**

$J_0$, $\Delta J_{ss}$, $\Delta u_2$ and $\Delta u_0$ can all be determined directly from the experimental data. The values are summarised in Table 3. Again, the value of $\Delta u_2$ is small in comparison with $\Delta u_0$, so there is no difference in the numerical prediction as to whether the length of the bridging law is given by $\Delta u_0$ or $\Delta u_0 + \Delta u_2$.

**Table 3. Numerical values for the bridging law for different composite systems**

<table>
<thead>
<tr>
<th>Composite system</th>
<th>$J_0$ [J/m$^2$]</th>
<th>$\Delta J_{ss}$ [J/m$^2$]</th>
<th>$K_{fac}$</th>
<th>$\Delta u_0$ [mm]</th>
<th>$\Delta u_1$ [mm]</th>
<th>$\Delta u_2$ [mm]</th>
</tr>
</thead>
<tbody>
<tr>
<td>sizing A/epoxy</td>
<td>300</td>
<td>3300</td>
<td>8.0</td>
<td>2.0</td>
<td>0.01</td>
<td>1.0</td>
</tr>
<tr>
<td>sizing A/polyester</td>
<td>130</td>
<td>4000</td>
<td>9.0</td>
<td>5.5</td>
<td>0.008</td>
<td>2.5</td>
</tr>
</tbody>
</table>

Figure 19 shows the resulting fit for the crack growth resistance versus crack opening where the value of $\Delta u_1$ has been adjusted to 1.0 and 2.5 mm for epoxy and polyester, respectively. In both cases, this is about half the value of the total crack opening, $\Delta u_0$.

![Graph](image)

**Figure 19. Comparison of crack growth resistance**

Figure 20 now shows the excellent fit between the numerical and experimental data for the crack growth resistance versus crack length. The s-shaped crack length curve is now predicted extremely well. For a further check of the proposed crack bridging shape, the predicted crack profiles are studied together with the experimental predictions. This was undertaken for the polyester specimens as seen in Figure 21.

The figure shows the crack opening profile for a crack length of 50 mm. For accuracy of the experimental crack opening measurements, an opening of at least 0.2 mm must be given. It can be seen that the bridging law also in this case leads to a very good agreement between the two data sets. Additionally, the unbridged crack profile is shown in the figure. The bridging stresses across the crack faces lead - as expected - to a decrease in the opening of the crack.
The maximum opening at the crack notch is - for a length of 50 mm - reduced to 25% from 2.0 to 1.5 mm. This is quite a considerable effect of the bridging stresses.

Figure 22 shows the results from above together with the numerical predictions for sizing A/epoxy at the same crack growth resistance of $J_R=3100\ \text{J/m}^2$. It can be seen that the crack opening and crack length for the same crack growth resistance are considerably smaller for sizing A/epoxy. Including the bridging stresses also changes the crack opening shape in the vicinity of the crack tip - however, this change is hard to determine correctly experimentally due to the small crack opening values.

Figure 23 now shows the numerical predictions for the crack opening profiles for both the sizing A/epoxy and sizing A/polyester system in comparison for the same crack opening. The curves are plotted for two different crack opening values of 0.3 and 1.2 mm. For these crack openings, sizing A/polyester gives the larger crack length together with a smaller crack growth resistance.
Figure 22. Comparison crack profiles for different bridging laws with $J_R = 3100 \text{ J/m}^2$

Figure 23. Comparison of crack profiles for different bridging laws

Unloading considerations
A possible procedure for unloading (i.e. crack closure during structural loading) was described earlier in Section 4.1 (see Figure 6). Although the current problem situation does not require an unloading procedure due to continually increasing crack opening, a theoretical consideration is given here. The splitting of the two fracture parts, $J_0$ and $\Delta J_{ss}$, can then also be used to change the history for elastic unloading: if we consider fracture initiation completed once the value of $\Delta u_2$ has been reached (void formation out of micropores), elastic unloading to the origin should only occur for openings larger than $\Delta u_2$. For smaller values, the path can simply be reversed. This has not been investigated in detail as the current problem does not require unloading.
7 Summary

This report describes the implementation of a cohesive user element into the commercial FE code ABAQUS. This element type proves to be a versatile tool in predicting opening, crack length and crack profiles as a function of the crack growth resistance. In comparison with the earlier implemented UINTER contact routine, the cohesive element has the distinct advantage that a higher number of integration points can be used to improve the numerical performance for a given mesh density. This reduces the computational time for the analysis greatly.

The experimentally measured bridging law was optimised with the help of the numerical predictions. Experimental data such as crack length and crack profiles were found to give very good agreement with the adjusted law. The parameters for the new law can mostly be determined from the experiments; only one parameter remains to be fitted.
References


A Implementation of the plane interface element

A.1 Derivation of force vector and stiffness matrix

The derivation of the force vector and stiffness matrix for the plane interface element are analogous to the linear element presented earlier.

The plane interface element has 48 (3x16) degrees of freedom. The nodal displacements in the global coordinate system are given as:

\[
\mathbf{d}_N = \begin{bmatrix} d_1^x \\ d_1^y \\ d_2^x \\ d_2^y \\ \vdots \\ d_{16}^x \\ d_{16}^y \end{bmatrix}, \quad (A.1)
\]

\[
\mathbf{u}_N = \mathbf{d}_N = \begin{bmatrix} 1 \\ 1 \\ 1 \\ \vdots \\ 1 \end{bmatrix} \begin{bmatrix} I_{24 	imes 24} \\ I_{24 	imes 24} \end{bmatrix}, \quad (A.2)
\]

where \( I_{24 	imes 24} \) denotes a unity matrix with 24 rows and columns.

Let \( N_i(\xi, \eta) \) be the shape functions for the node pair \( i (i = 1, \ldots, 8) \), where \( \xi \) and \( \eta \) stand for the local coordinates of the element with \(-1 \leq \xi \leq 1 \) and \(-1 \leq \eta \leq 1 \). The relative displacement between the nodes for each point within the element is then given by:

\[
\Delta \mathbf{u}(\xi, \eta) = \begin{bmatrix} \Delta u_x(\xi, \eta) \\ \Delta u_y(\xi, \eta) \\ \Delta u_z(\xi, \eta) \end{bmatrix} = \mathbf{H}(\xi, \eta) \Delta \mathbf{d}_N, \quad (A.3)
\]

where \( \mathbf{H}(\xi, \eta) \) is a 3x24 matrix containing the individual shape functions. It has the form

\[
\mathbf{H}(\xi, \eta) = (N_1(\xi, \eta) \mathbf{I}_{3x3} \mid N_2(\xi, \eta) \mathbf{I}_{3x3} \mid \cdots \mid N_8(\xi, \eta) \mathbf{I}_{3x3}). \quad (A.4)
\]

for plane interface elements.

As a result, we get

\[
\Delta \mathbf{u}(\xi, \eta) = \mathbf{H} \Phi \mathbf{d}_N = \mathbf{B} \mathbf{d}_N, \quad (A.5)
\]

where \( \mathbf{B} \) is of the dimension 3x48 and \( \Delta \mathbf{u} \) of the dimension 3x1.

The coordinates of any specific reference position can be derived according to Eq. (A.3):

\[
\mathbf{x}_R(\xi, \eta) = \mathbf{H}(\xi, \eta) \mathbf{x}_N^R \quad (A.6)
\]

The tangential plane is spanned by two vectors, \( \mathbf{v}_\xi \) and \( \mathbf{v}_\eta \). They are, at a given point, obtained by differentiating the global position vector with respect to the local coordinates. Although \( \mathbf{v}_\xi \) and \( \mathbf{v}_\eta \) are generally not orthogonal to each other, their vector product defines the surface normal. Therefore, the local normal vector is obtained by:

\[
\mathbf{t}_n = \frac{1}{\| \frac{\partial \mathbf{x}_R}{\partial \xi} \times \frac{\partial \mathbf{x}_R}{\partial \eta} \|} \left( \frac{\partial \mathbf{x}_R}{\partial \xi} \times \frac{\partial \mathbf{x}_R}{\partial \eta} \right) \quad (A.7)
\]

The tangential coordinates are defined as:

\[
\mathbf{t}_1 = \frac{1}{\| \frac{\partial \mathbf{x}_R}{\partial \xi} \|} \frac{\partial \mathbf{x}_R}{\partial \xi} \quad (A.8)
\]

\[
\mathbf{t}_2 = \mathbf{t}_n \times \mathbf{t}_1 \quad (A.9)
\]

The components \( \mathbf{t}_1, \mathbf{t}_2 \) and \( \mathbf{t}_n \) represent the direction cosines of the local coordinate system to the global one, thus defining the 3 \times 3 transformation tensor \( \mathbf{\Theta} \):

\[
\mathbf{\Theta} = (\mathbf{t}_1, \mathbf{t}_2, \mathbf{t}_n). \quad (A.10)
\]

The local displacements are then obtained by

\[
\Delta \mathbf{u}_{\text{loc}} = \mathbf{\Theta}^T \Delta \mathbf{u}. \quad (A.11)
\]
The element stiffness matrix and the right hand side nodal force vector are required for the UEL subroutine in ABAQUS.

The nodal force vector is defined as

\[
\mathbf{f}_{el}^N = \int_{-1}^{1} \int_{-1}^{1} \mathbf{B}^T \mathbf{\Theta}^T \mathbf{t}_{\text{loc}} \det J \, d\xi \, d\eta, \tag{A.12}
\]

where \( \mathbf{t}_{\text{loc}} \) is the 3x1 vector defining the bridging stresses. The bridging stresses are connected to the opening displacements by the cohesive law as derived previously. It should be noticed that - for numerical purposes - the relationship can be expressed in various forms (analytical, piecewise linear, etc).

\( \det J \) is the Jacobian defined by the transformation of the current element coordinates \((\xi, \eta)\) to the global coordinates \((x, y, z)\), and defined as

\[
\begin{align*}
\det J &= \sqrt{(\det J1)^2 + (\det J2)^2 + (\det J3)^2} \quad \text{with} \\
\det J1 &= \frac{\partial x_R}{\partial \xi} \frac{\partial y_R}{\partial \eta} - \frac{\partial x_R}{\partial \eta} \frac{\partial y_R}{\partial \xi} \tag{A.13} \\
\det J2 &= \frac{\partial x_R}{\partial \xi} \frac{\partial z_R}{\partial \eta} - \frac{\partial x_R}{\partial \eta} \frac{\partial z_R}{\partial \xi} \tag{A.14} \\
\det J3 &= \frac{\partial y_R}{\partial \xi} \frac{\partial z_R}{\partial \eta} - \frac{\partial y_R}{\partial \eta} \frac{\partial z_R}{\partial \xi} \tag{A.15}
\end{align*}
\]

Note that depending on the orientation of the local to the global coordinate system, the components \( \det J1, \det J2 \) and \( \det J3 \) can be negative.

The tangent stiffness matrix (note sign convention for ABAQUS) is defined as

\[
\mathbf{K}^{el} = - \frac{\partial \mathbf{f}_{el}^N}{\partial \mathbf{d}^e} = - \int_{e} \mathbf{B}^T \mathbf{\Theta}^T \mathbf{D}_{\text{loc}} \mathbf{\Theta} \mathbf{B} \, dS_e \tag{A.17}
\]

\[
\mathbf{K}_{48 \times 48} = - \int_{-1}^{1} \int_{-1}^{1} \mathbf{B}^T \mathbf{\Theta}^T \mathbf{D}_{\text{loc}} \mathbf{\Theta} \mathbf{B} \, d\xi \, d\eta \tag{A.18}
\]

### A.2 State variables for the analysis

The number of state variables for each element depends on the chosen integration procedure. At each integration point of the plane element, there are three state variables for the relative displacement (x-, y- and z-direction) and the local stresses acting in the x-, y- and z-direction. Furthermore, there is one value describing the current interaction state between the two surfaces for the purpose of stability analysis and position tracking during the numerical procedure. For a standard integration procedure with 9 integration points, this results in 63 (9x7) state variables for the plane element. As ABAQUS will not terminate the analysis if an insufficient number of state variables is provided for the chosen integration procedure, this user input error is checked within the UEL subroutine.
B Quadratic shape functions and derivatives

B.1 Line element

The three quadratic shape functions for line element are as follows:

\[ N_1(\xi) = \frac{1}{2}(-\xi + \xi^2) \quad \text{(B.1)} \]
\[ N_2(\xi) = \frac{1}{2}(\xi + \xi^2) \quad \text{(B.2)} \]
\[ N_3(\xi) = 1 - \xi^2 \quad \text{(B.3)} \]

Derivative of shape functions

\[ N_{1,\xi}(\xi) = -\frac{1}{2} + \xi \quad \text{(B.4)} \]
\[ N_{2,\xi}(\xi) = \frac{1}{2} + \xi \quad \text{(B.5)} \]
\[ N_{3,\xi}(\xi) = -2\xi \quad \text{(B.6)} \]

B.2 Plane element

The quadratic shape functions for the plane element are given below:

\[ N_1(\xi, \eta) = \frac{1}{4}(1 - \xi)(1 - \eta) - \frac{1}{2}(N_5 + N_6) \quad \text{(B.7)} \]
\[ N_2(\xi, \eta) = \frac{1}{4}(1 + \xi)(1 - \eta) - \frac{1}{2}(N_5 + N_6) \quad \text{(B.8)} \]
\[ N_3(\xi, \eta) = \frac{1}{4}(1 + \xi)(1 + \eta) - \frac{1}{2}(N_6 + N_7) \quad \text{(B.9)} \]
\[ N_4(\xi, \eta) = \frac{1}{4}(1 - \xi)(1 + \eta) - \frac{1}{2}(N_7 + N_8) \quad \text{(B.10)} \]
\[ N_5(\xi, \eta) = \frac{1}{2}(1 - \xi^2)(1 - \eta) \quad \text{(B.11)} \]
\[ N_6(\xi, \eta) = \frac{1}{2}(1 + \xi)(1 - \eta^2) \quad \text{(B.12)} \]
\[ N_7(\xi, \eta) = \frac{1}{2}(1 - \xi^2)(1 + \eta) \quad \text{(B.13)} \]
\[ N_8(\xi, \eta) = \frac{1}{2}(1 - \xi)(1 - \eta^2) \quad \text{(B.14)} \]
Derivative of shape functions

\[ N_{1,\xi} = -\frac{1}{4}(1 - \eta) - \frac{1}{2}(N_{5,\xi} + N_{8,\xi}) \] (B.15)

\[ N_{2,\xi} = \frac{1}{4}(1 - \eta) - \frac{1}{2}(N_{5,\xi} + N_{6,\xi}) \] (B.16)

\[ N_{3,\xi} = \frac{1}{4}(1 + \eta) - \frac{1}{2}(N_{6,\xi} + N_{7,\xi}) \] (B.17)

\[ N_{4,\xi} = -\frac{1}{4}(1 + \eta) - \frac{1}{2}(N_{7,\xi} + N_{8,\xi}) \] (B.18)

\[ N_{5,\xi} = -\xi(1 - \eta) \] (B.19)

\[ N_{6,\xi} = \frac{1}{2}(1 - \eta^2) \] (B.20)

\[ N_{7,\xi} = -\xi(1 + \eta) \] (B.21)

\[ N_{8,\xi} = -\frac{1}{2}(1 - \eta^2) \] (B.22)

\[ N_{1,\eta} = -\frac{1}{4}(1 - \xi) - \frac{1}{2}(N_{5,\eta} + N_{8,\eta}) \] (B.23)

\[ N_{2,\eta} = -\frac{1}{4}(1 + \xi) - \frac{1}{2}(N_{5,\eta} + N_{6,\eta}) \] (B.24)

\[ N_{3,\eta} = \frac{1}{4}(1 + \xi) - \frac{1}{2}(N_{6,\eta} + N_{7,\eta}) \] (B.25)

\[ N_{4,\eta} = \frac{1}{4}(1 - \xi) - \frac{1}{2}(N_{7,\eta} + N_{8,\eta}) \] (B.26)

\[ N_{5,\eta} = -\frac{1}{2}(1 - \xi^2) \] (B.27)

\[ N_{6,\eta} = -\eta(1 + \xi) \] (B.28)

\[ N_{7,\eta} = \frac{1}{2}(1 - \xi^2) \] (B.29)

\[ N_{8,\eta} = -\eta(1 - \xi) \] (B.30)
C Integration points and weights

C.1 Gauss integration point positions and weights

3 points

\[ p_1 = -\sqrt{0.6} \quad w_1 = 0.555 \]
\[ p_2 = 0.0 \quad w_2 = 0.888 \]
\[ p_3 = \sqrt{0.6} \quad w_3 = 0.555 \]

6 points

\[ p_1 = -0.9324695142031520 \quad w_1 = 0.1713244923791709 \]
\[ p_2 = -0.6612093864662646 \quad w_2 = 0.3607615730481379 \]
\[ p_3 = -0.2386191860831968 \quad w_3 = 0.4679139345726913 \]
\[ p_4 = 0.2386191860831968 \quad w_4 = 0.4679139345726913 \]
\[ p_5 = 0.6612093864662646 \quad w_5 = 0.3607615730481379 \]
\[ p_6 = 0.9324695142031520 \quad w_6 = 0.1713244923791709 \]

12 points

\[ p_1 = -0.981560634246732 \quad w_1 = 0.04717533638647547 \]
\[ p_2 = -0.904117256370452 \quad w_2 = 0.10693932599553637 \]
\[ p_3 = -0.7699026741943177 \quad w_3 = 0.1600783285433586 \]
\[ p_4 = -0.5873179542866143 \quad w_4 = 0.2031674267230672 \]
\[ p_5 = -0.3678314989981804 \quad w_5 = 0.2334925365383534 \]
\[ p_6 = -0.12523340851114688 \quad w_6 = 0.2491470458134027 \]
\[ p_7 = 0.12523340851114688 \quad w_7 = 0.2491470458134027 \]
\[ p_8 = 0.3678314989981804 \quad w_8 = 0.2334925365383534 \]
\[ p_9 = 0.5873179542866143 \quad w_9 = 0.2031674267230672 \]
\[ p_{10} = 0.7699026741943177 \quad w_{10} = 0.1600783285433586 \]
\[ p_{11} = 0.904117256370452 \quad w_{11} = 0.10693932599553637 \]
\[ p_{12} = 0.981560634246732 \quad w_{12} = 0.04717533638647547 \]
C.2 Newton-Cotes integration point positions and weights

3 points

\[ \begin{align*}
    p_1 &= -1.0 \quad w_1 = 0.333 \\
    p_2 &= 0.0 \quad w_2 = 1.333 \\
    p_3 &= 1.0 \quad w_3 = 0.333
\end{align*} \]

6 points

\[ \begin{align*}
    p_1 &= -1.0 \quad w_1 = 19/144 \\
    p_2 &= -3/5 \quad w_2 = 75/144 \\
    p_3 &= -1/5 \quad w_3 = 50/144 \\
    p_4 &= 1/5 \quad w_4 = 50/144 \\
    p_5 &= 3/5 \quad w_5 = 75/144 \\
    p_6 &= 1.0 \quad w_6 = 19/144
\end{align*} \]

12 points

\[ \begin{align*}
    p_1 &= -1.0 \quad w_1 = 2171465/43545600 \\
    p_2 &= -9/11 \quad w_2 = 13486539/43545600 \\
    p_3 &= -7/11 \quad w_3 = -3237113/43545600 \\
    p_4 &= -5/11 \quad w_4 = 25226685/43545600 \\
    p_5 &= -3/11 \quad w_5 = -9595542/43545600 \\
    p_6 &= -1/11 \quad w_6 = 15493566/43545600 \\
    p_7 &= 1/11 \quad w_7 = 15493566/43545600 \\
    p_8 &= 3/11 \quad w_8 = -9595542/43545600 \\
    p_9 &= 5/11 \quad w_9 = 25226685/43545600 \\
    p_{10} &= 7/11 \quad w_{10} = -3237113/43545600 \\
    p_{11} &= 9/11 \quad w_{11} = 13486539/43545600 \\
    p_{12} &= 1.0 \quad w_{12} = 2171465/43545600
\end{align*} \]

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D  ABAQUS coding for the quadratic line element

The following code will be compiled and linked when calling the ABAQUS job. For unix systems, the command prompt will be as follows:

```
abaqus job=jobname user=interface
```

This syntax is of course dependent on the platform you are using. For further information see the ABAQUS manuals. The following code for the user subroutine UEL is written in FORTRAN 77 and saved as 'interface.f'.

```
SUBROUTINE UEL(RHS, AMATRX, SVARS, ENERGY, NDOFEL, NRHS, NSVARS,
1 PROPS, NPROPS, COORDS, MCRD, NNODE, U, DU, V, A, JTYPE, TIME, DTIME,
2 KSTEP, KINC, JELEM, PARAMS, NDLOAD, JDLTYP, ADLMAG, PREDEF,
3 NPRED, LFLAGS, MLVARX, DDLMAG, MDLOAD, PMNEDT, JPROPS, NJPROP,
4 PERIOD)

C
INCLUDE 'ABA_PARAM.INC'
PARAMETER (ZERO = 0.D0, HALF=0.5D0, ONE=1.0D0, TWO=2.0d0,
1 THREE= 3.0d0, TOL=-1E-5)
DIMENSION RHS(MLVARX,*), AMATRX(NDOFEL,NDOFEL),
1 SVARS(NSVARS), ENERGY(8), PROPS(*), COORDS(MCRD,NNODE),
2 U(NDOFEL), DU(MLVARX,*), V(NDOFEL), A(NDOFEL), TIME(2),
3 PARAMS(3), JDLTYP(MDLOAD,*), ADLMAG(MDLOAD,*),
4 MDLOAD(MDLOAD,*), PREDEF(2,NPRED,NNODE), LFLAGS(*),
5 JPROPS(*)

C GENERAL ELEMENT VALUES
DIMENSION STRESS(MCRD)
DIMENSION DDSDDR(MCRD)
C GAUSS INTEGRATION VARIABLES (3 INTEG POINT)
DIMENSION GAUSS3(3), WEIGHT3(3), COTNEW(3), CWEIGHT(3)
DIMENSION GAUSS6(6), WEIGHT6(6), COTNEW6(6), CW6(6)
DIMENSION GAUSS12(12), WEIGHT12(12), COTNEW12(12), CW12(12)
C ARRAYS FOR QUADRATIC LINE ELEMENT
DIMENSION DNDXI(3), DELTA_U(6), DU_CONT(MCRD), DU_LOC(MCRD)
DIMENSION H(MCRD,6), C_COOR(MCRD,NNODE), PSI(6,NDOFEL)
DIMENSION B(MCRD, NDOFEL), BT(NDOFEL, MCRD)
DIMENSION A1(NDOFEL, MCRD), A2(NDOFEL, NDOFEL)
DIMENSION AV_COOR(MCRD,5), V_XI(MCRD), V_N(MCRD)
DIMENSION THETA(MCRD, MCRD), STR_GLOB(MCRD)
DIMENSION D_GLOB(MCRD, MCRD), DD1(MCRD, MCRD)
data iuel/0/
save iuel

C
C QUADRATIC LINE ELEMENT
C SVARS - In 1, contains the lOpenClose identifier
C - In 3-4, contains the traction stiffness
C - In 5-6, contains the traction opening
C
C INITIALISATION: IMPORTANT!! FORTRAN DOES NOT PUT ZEROS IN THERE AUTOMATICALLY
CALL KASET2(AMATRX, NDOFEL, NDOFEL)
IF (NRHS.EQ.1) THEN
  CALL KASET1(RHS, MLVARX)
ELSE
  CALL KASET2(RHS, MLVARX, NRHS)
END IF

CALL KASET2(PSI, 6, NDOFEL)
```

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CALL KASET2(H, MCRD, 6)
CALL KASET2(AV_COOR, MCRD, 3)
CALL KASET1(V_XI, MCRD)
CALL KASET1(V_N, MCRD)
CALL KASET2(THETA, MCRD, MCRD)
CALL KASET2(DDSDDR, MCRD, MCRD)
CALL KASET2(D_GLOB, MCRD, MCRD)
CALL KASET1(STRESS, MCRD)
CALL KASET1(STR_GLOB, MCRD)

C REAL INPUT PROPERTIES
WIDTH = PROPS(7) ! Width of elements (same as solid section width for solid elements)

C INTEGER INPUT PROPERTIES
NINTP = JPROPS(1) ! Number of integration points
INTS = JPROPS(2) ! Integration point scheme (1: gauss, 2: newton cotes)

C INFORMATION OUTPUT AND CHECK
IF (iuel.EQ.0) THEN
OPEN(15,FILE=
1 '/user/feih/abaqus/uel/verify.out')
WRITE(7,*) 'First call to UEL-------------------'
WRITE(7,*) 'DEGREES OF FREEDOM:',NDOFEL
WRITE(7,*) 'number of nodes:', NNODE
WRITE(7,*) 'number of integration points:', NINTP
WRITE(7,*) 'Integration scheme:', INTS
WRITE(7,*) 'maximum coords:', MCRD
WRITE(7,*) 'number of variables:', NSVARS
WRITE(7,*) 'number of real properties', NPROPS
WRITE(7,*) 'number of integer properties', NJPROP
WRITE(7,*) 'dimensioning parameter:', MLVARX
write(7,*) 'KINC:', KINC
write(7,*) 'LFLAGS(1)=', LFLAGS(1)
write(7,*) 'LFLAGS(2)=', LFLAGS(2)
write(7,*) 'LFLAGS(3)=', LFLAGS(3)
write(7,*) 'LFLAGS(4)=', LFLAGS(4)
write(7,*) 'LFLAGS(5)=', LFLAGS(5)

C CHECKING FOR THE RIGHT NUMBER OF NODES
IF (NNODE.NE.6) THEN
CALL STDB_ABQERR(-3, '6 nodes required for interface element: 1 specified number of nodes is incorrect',0,0.0,' ')
END IF

C Checking for number of state variables
minnum = NINTP*5
IF (NSVARS.LT.minnum) THEN
CALL STDB_ABQERR(-3, 'Number of state variables too small for 1 chosen number of integration points!',MINNUM,0.0,' ')
END IF

IUEL = 1

C WRITE(7,*) 'New call to UEL'
C do k=1, NDOFEL
C write(7,*) 'U', U(k)
C end do
call flush_(15)

C Calculate relation matrix
C Calculate relative opening at node pairs
C DEFINE DELTA_U=U_TOP - U_BOTTOM

DO 10 K = 1, NDOFEL/2
    PSI(K, K) = -ONE
    PSI(K, K+NDOFEL/2) = ONE
10 END DO

C Compute nodal coordinates in deformed state
C ADD PROPER COORDINATE TRANSFORMATION LATER

DO 20 I=1,MCRD
    DO 30 J=1, NNODE
        NN=I+(J-1)*MCRD
        C_COOR(I,J)=COORDS(I,J) + U(NN)
    30 END DO
20 END DO

C Reference coordinate system (midpoint averages)

DO 31 I=1, MCRD
    DO 32 J=1, NNODE/2
        AV_COOR(I,J)=ONE/TWO*(C_COOR(I,J)+C_COOR(I,J+NNODE/2))
    32 END DO
31 END DO

c Gaussian integration (3 gauss points)
GAUSS3(1) = -SQRT(0.6)
GAUSS3(2) = ZERO
GAUSS3(3) = SQRT(0.6)

WEIGHT3(1) = 0.5555555555555555
WEIGHT3(2) = 0.88888888888888
WEIGHT3(3) = 0.5555555555555555

c Gaussian integration (6 gauss points)
GAUSS6(1) = -0.932469514203152
GAUSS6(2) = -0.6612093864662646
GAUSS6(3) = -0.2386191860831968
GAUSS6(4) = 0.2386191860831968
GAUSS6(5) = 0.6612093864662646
GAUSS6(6) = 0.932469514203152

WEIGHT6(1) = 0.1713244923791709
WEIGHT6(2) = 0.3607615730481379
WEIGHT6(3) = 0.4679139345726913
WEIGHT6(4) = 0.4679139345726913
WEIGHT6(5) = 0.3607615730481379
WEIGHT6(6) = 0.1713244923791709

c Gaussian integration (12 gauss points)
GAUSS12(1) = -0.981560634246732
GAUSS12(2) = -0.904117256370452
GAUSS12(3) = -0.7699026741943177
GAUSS12(4) = -0.5873179542866143
GAUSS12(5) = -0.3678314989981804
GAUSS12(6) = -0.12523340851114688
GAUSS12(7) = 0.12523340851114688
GAUSS12(8) = 0.3678314989981804
GAUSS12(9) = 0.5873179542866143
GAUSS12(10) = 0.7699026741943177
GAUSS12(11) = 0.904117256370452
GAUSS12(12) = 0.981560634246732

WEIGHT12(1) = 0.04717533638647547
WEIGHT12(2) = 0.1069393259953637
WEIGHT12(3) = 0.1600783285433586
WEIGHT12(4) = 0.2031674267230672
WEIGHT12(5) = 0.2334925365383534
WEIGHT12(6) = 0.2491470458134027
WEIGHT12(7) = 0.2491470458134027
WEIGHT12(8) = 0.2334925365383534
WEIGHT12(9) = 0.2031674267230672
WEIGHT12(10) = 0.1600783285433586
WEIGHT12(11) = 0.1069393259953637
WEIGHT12(12) = 0.04717533638647547

c Newton Cotes integration (3 integration points)
COTNEX(1) = -ONE
COTNEX(2) = ZERO
COTNEX(3) = ONE
CWEIGHT(1) = ONE/THREE
CWEIGHT(2) = ONE + ONE/THREE
CWEIGHT(3) = ONE/THREE

c Newton Cotes integration (6 integration points)
COTNEX6(1) = -ONE
COTNEX6(2) = -3.0d0/5.0d0
COTNEX6(3) = -1.0d0/5.0d0
COTNEX6(4) = 1.0d0/5.0d0
COTNEX6(5) = 3.0d0/5.0d0
COTNEX6(6) = ONE
CW6(1) = 19.0d0/144.0d0
CW6(2) = 75.0d0/144.0d0
CW6(3) = 50.0d0/144.0d0
CW6(4) = 50.0d0/144.0d0
CW6(5) = 75.0d0/144.0d0
CW6(6) = 19.0d0/144.0d0

c Newton Cotes integration (12 integration points)
COTNEX12(1) = -ONE
COTNEX12(2) = -9.0d0/11.0d0
COTNEX12(3) = -7.0d0/11.0d0
COTNEX12(4) = -5.0d0/11.0d0
COTNEX12(5) = -3.0d0/11.0d0
COTNEX12(6) = -1.0d0/11.0d0
COTNEX12(7) = 1.0d0/11.0d0
COTNEX12(8) = 3.0d0/11.0d0
COTNEX12(9) = 5.0d0/11.0d0
COTNEX12(10) = 7.0d0/11.0d0
COTNEX12(11) = 9.0d0/11.0d0
COTNEX12(12) = ONE

CW12(1) = 2171465.0d0/43545600.0d0
CW12(2) = 13486539.0d0/43545600.0d0
CW12(3) = -3237113.0d0/43545600.0d0
CW12(4) = 25226685.0d0/43545600.0d0
CW12(5) = -9595542.0d0/43545600.0d0
CW12(6) = 15493566.0d0/43545600.0d0
CW12(7) = 15493566.0d0/43545600.0d0
CW12(8) = -9595542.0d0/43545600.0d0
CW12(9) = 25226685.0d0/43545600.0d0
CW12(10) = -3237113.0d0/43545600.0d0
CW12(11) = 13486539.0d0/43545600.0d0
CW12(12) = 2171465.0d0/43545600.0d0
C IF (LFLAGS(3).EQ.1) THEN
C Normal incrementation (RHS and AMATRX required)
C IF (LFLAGS(1).EQ.1.OR.LFLAGS(1).EQ.2) THEN
C *STATIC AND *STATIC, DIRECT
C LOOP OVER INTEGRATION POINTS
DO 100 IINTP = 1,NINTP
C Shape function value
H1 = ONE/TWO*(-POINT + POINT**TWO)
H2 = ONE/TWO*( POINT + POINT**TWO)
H3 = ONE - POINT**TWO
H matrix
H(1,1) = H1
H(2,2) = H1
H(1,3) = H2
H(2,4) = H2
H(1,5) = H3
H(2,6) = H3
C END IF
C DERIVATIVE OF SHAPE FUNCTION VALUE (3X1 MATRIX)
DNDXI(1) = -ONE/TWO + POINT
DNDXI(2) = ONE/TWO + POINT
DNDXI(3) = -TWO*POINT
C write(7,**) 'Starting loop over integration points'
c write(7,*), 'INTP POINT and WEIGHT', IINTP, POINT, WEIGHT
C call flush_(7)

CALL KASET2(B, MCRD, NDOFEL)

DO 110 I=1, MCRD
   DO 120 J=1, NDOFEL
      DO 130 K=1, NDOFEL/2
         B(I,J) = B(I,J) + H(I,K)*PSI(K,J)
      130 END DO
   120 END DO
110 END DO

C TRANSPOSED B MATRIX
DO 140 I=1, MCRD
   DO 150 J=1, NDOFEL
      BT(J,I) = B(I,J)
   150 END DO
140 END DO

C CALCULATE GLOBAL DISPLACEMENT AT INTEGRATION POINT
C FROM CONTINUOUS DISPLACEMENT
CALL KASET1(DU_CONT, MCRD)

DO 160 I=1, MCRD
   DO 170 J=1, NDOFEL
      DU_CONT(I) = DU_CONT(I) + B(I,J)*U(J)
   170 END DO
160 END DO

C LOCAL COORDINATE SYSTEM
C (USE AVERAGE OF DEFORMED X-POSITIONS OF TOP AND BOTTOM)
X_xi = ZERO
Y_xi = ZERO

DO 180 I=1,3
   X_xi = X_xi +
   DNDXI(I)*AV_COOR(1,I)

   Y_xi = Y_xi +
   DNDXI(I)*AV_COOR(2,I)
180 END DO

c Jacobian (vector length in xi-direction)
DETJ = sqrt(X_xi**TWO + Y_xi**TWO)

IF (DETJ.LT.ZERO) THEN
   write(7,*), 'Negative Jacobian encountered! Check element and nodal definition for elem', JELEM
   CALL XIT
END IF

C Local coordinate vector
V_XI(1) = X_xi/DETJ
V_XI(2) = Y_xi/DETJ

C Normal vector in 90 degree angle
V_N(1) = - V_XI(2)
V_N(2) = V_XI(1)
Rotational matrix

\[ \begin{align*}
\theta_{1,1} &= v_{x1} \\
\theta_{2,1} &= v_{x2} \\
\theta_{1,2} &= v_{n1} \\
\theta_{2,2} &= v_{n2}
\end{align*} \]

Relative displacement in local coordinate system

```fortran
CALL KASET1(DU_LOC, MCRD)
DO 181 I=1, MCRD
   DO 182 J=1, MCRD
      DU_LOC(I) = DU_LOC(I) + THETA(J,I) * DU_CONT(J)
   182 END DO
181 END DO
```

Over-closure check (can be used as re-start criterion - see uinter)

```fortran
IF (DU_LOC(2).LT.TOL) THEN
   WRITE(7,*) 'Over-closure at element', JELEM
END IF
```

C write (7,*) 'DU_LOC:', DU_LOC(1), DU_LOC(2), IINTP
C CALL FLUSH_(7)
C
Calculate stress and traction stiffness based on relative displacement

```fortran
CALL KTRACN(DU_LOC, PROPS, STRESS, DDSDDR,
           MCRD, SVARS, NSVARS, IINTP, NINTP, KINC, JELEM)
```

dummy stiffness for friction (no influence under mode I opening when coupled with equation)
for accuracy there should be coupling terms,
but again: no influence under mode I opening

\[ \text{DDSSDDR}(1,1) = 10000 \]

RHS assembly

```fortran
IF (NDLOAD.NE.0) THEN
   WRITE(7,*) 'Element loads not implemented'
   CALL FLUSH_(7)
   CALL XIT
END IF
```

Stiffness matrix

Transformation

```fortran
CALL KASET2(DD1, MCRD, MCRD)
DO 183 I=1, MCRD
   DO 184 J=1, MCRD
      DO 185 K=1, MCRD
         DD1(I,J) = DD1(I,J) + DDSDDR(I,K) * THETA(J,K)
      185 END DO
184 END DO
183 END DO
```

```fortran
CALL KASET2(D_GLOB, MCRD, MCRD)
DO 186 I=1, MCRD
   DO 187 J=1, MCRD
      DO 188 K=1, MCRD
         D_GLOB(I,J) = D_GLOB(I,J) + THETA(I,K) * DD1(K,J)
      188 END DO
187 END DO
186 END DO
```
CALL KASET2 (A1, NDOFEL, MCRD)
DO 190 I=1, NDOFEL
   DO 191 J=1, MCRD
      DO 192 K=1, MCRD
         A1(I,J) = A1(I,J) + BT(I,K)*D_GLOB(K,J)
   192 END DO
191 END DO
190 END DO

CALL KASET2 (A2, NDOFEL, NDOFEL)
DO 195 I=1, NDOFEL
   DO 196 J=1, NDOFEL
      DO 197 K=1, MCRD
197 END DO
196 END DO
195 END DO

DO 200 I=1, NDOFEL
   DO 201 J=1, NDOFEL
      AMATRX(I,J) = AMATRX(I,J) + WIDTH*WEIGHT*DETJ*A2(I,J)
201 END DO
200 END DO

C Right hand side
C Transformation
CALL KASET1(STR_GLOB, MCRD)
DO 202 I=1, MCRD
   DO 203 J=1, MCRD
      STR_GLOB(I) = STR_GLOB(I) + THETA(I,J)*STRESS(J)
203 END DO
202 END DO

DO 230 I=1, NDOFEL
   DO 240 K=1,MCRD
      RHS(I,1) = RHS(I,1) + DETJ*WIDTH*WEIGHT*BT(I,K)*STR_GLOB(K)
240 END DO
230 END DO

IF (NRHS.EQ.2) THEN
   WRITE(7,*) 'Riks solution not supported by element'
   CALL FLUSH_(7)
   CALL XIT
END IF

IF (LFLAGS(4).EQ.1) THEN
   C PERTURBATION STEP
   WRITE(7,*) 'Perturbation step not supported by element'
   CALL FLUSH_(7)
ENDIF
CALL XIT
END IF

C
SAVE OPENING AND STRESSES AT INTEGRATION POINT AS STATE VARIABLES
SVARS(IINTP+NINTP) = DU_LOC(1)
SVARS(IINTP+2*NINTP) = DU_LOC(2)
SVARS(IINTP+3*NINTP) = STRESS(1)
SVARS(IINTP+4*NINTP) = STRESS(2)
100 END DO

ELSE
  WRITE(7,*) 'Only static procedure supported by element'
  CALL FLUSH_(7)
  CALL XIT
END IF

ELSE IF (LFLAGS(3).EQ.4) THEN
  DO I=1, NDOFEL
    AMATRX(I,I)= 1.0d0
  END DO

ELSE
  WRITE(7,*) 'Only normal incrementation supported by element'
  CALL FLUSH_(7)
  CALL XIT
END IF
RETURN
END

C-----------------------------------------------------------------------------------
subroutine ktracn(RDISP, PROPS, STRESS, DDSDDR, MCRD, SVARS,
1 NSVARS, IINTP, NINTP, KINC, JELEM)
INCLUDE 'ABA_PARAM.INC'
PARAMETER (ZERO = 0.D0, TWO=2.0D0, ONE= 1.0D0, THREE= 3.0d0)
DIMENSION PROPS(*), RDISP(MCRD), STRESS(MCRD), DDSDDR(MCRD, MCRD)
DIMENSION SVARS(NSVARS)
data ifirst/0/
data iopen/0/
data iclose/0/
save ifirst, nodefirst, iopen, iclose

c REAL INPUT PROPERTIES
dJss = props(1) !Increase in fracture toughness
deltac = props(2) !Max crack bridging opening
delta1 = props(3) !Initial linear decrease/increase, softening afterwards
dJ0 = props(4) !Value of J0 (from measurements)
fac1 = props(5) !Stress increase factor for power law
penalty = props(6) !Penalty factor on contact
sigma0 = 1.5d0*dJss/delta1*sqrt(delta1/deltac)
slope1 = -dJss/(delta1*deltac*sqrt(delta1/deltac))
fac = dJss/(two*sqrt(deltac))
sigma1 = fac/sqrt(deltac)
slope = sigma1/delta1

c J0 is included separately now

c with zero start power law
alpha = 100.0
\[ \delta_2 = \frac{dJ_0}{\sigma_0 \cdot \text{fac1}} \cdot \frac{(\alpha + 1)}{\alpha} \]

Code checks for change in opening status. If too many contact points change status,
c increment can be restarted
c Checks for opening/closing behaviour
C get values from state variables

\[ \text{iold} = \text{SVARS(IINTP)} \]
\[ \text{stressold} = \text{SVARS(IINTP + 4*NINTP)} \]
\[ \text{rdispold} = \text{SVARS(IINTP + 2*NINTP)} \]

c Code checks for change in opening status. If too many contact points change status,
c increment can be restarted
c Checks for opening/closing behaviour

IF (ifirst.eq.0) THEN
  ifirst = 1
  NODEFIRST = NODE
  write(7,*) dJss, deltac, delta1, dJ0, fac1, penalty
  write(7,*) sigma0, sigma1, slope, slope1, delta2, fac
END IF

c new increment detection (includes restart) to count contact changes
IF (NODE.EQ.NODEFIRST.AND.KIT.EQ.1) THEN
  iopen = 0
  iclose = 0
END IF

c Check for increasing opening displacement (start in increment 2,
c once all contact points are closed)
c Only applies in opening stage (rdisp < 0)
c Not included right now (KINC.GE.1000)
IF (rdisp(2).LT.rdispold.AND.rdisp(2).GT.delta2.AND.
  KINC.GE.1000) THEN
  Elastic unloading and reloading
  stress(2) = stressold/rdispold*rdisp(2)
  ddsdrr(2,2) = stressold/rdispold
  write(7,*) 'Elastic unloading encountered'
ELSE
  Check for penetration of surfaces and indicate status
  if (rdisp(2).LT.zero) then
    write(7,*) 'Area I'
    stress(2) = penalty*slope*rdisp(2)
    ddsdrr(2,2) = penalty*slope
    lOpenClose = 0
  c
  c Check for opening of crack
  c Stresses will be negative (tension)
  c First slope bit (different from square root law)
  else if (rdisp(2).GE.zero.and.rdisp(2).LT.delta2) then
    Initial increase
    write(7,*) 'Area II'
    stress(2) = fac1*\sigma_0^*\alpha^*
    1
    \[ 1.0-((\delta_2 - \text{rdisp}(2))/\delta_2)^{\alpha} \]
    ddsdrr(2,2) = fac1*alpha*sigma0/delta2*
    1
    \[ ((\delta_2 - \text{rdisp}(2))/\delta_2)^{\alpha} \]
    lOpenClose = 1

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c Softening behaviour

else if (rdisp(2).GE.delta2.and.
1 rdisp(2).LT.(delta1+delta2)) then

c write(7,*) 'Area III'
stress(2) = sigma0 + slope1*(rdisp(2)-delta2)
ddsddr(2,2) = slope1
lOpenClose = 2

else if (rdisp(2).GE.(delta1+delta2).
1 and.rdisp(2).LT.(deltac+delta2)) then

c write(7,*) 'Area IV'
stress(2) = fac/sqrt((rdisp(2)-delta2))
ddsddr(2,2) = -fac/2*((rdisp(2)-delta2))**(-3.d0/2.d0)
lOpenClose = 3

else if (rdisp(2).GT.(deltac+delta2)) then

c write(7,*) 'Area V'
stress(2) = 0
ddsddr(2,2) = 0
lOpenClose = 4

c end if

END IF

IF (lOpenClose.NE.iold.AND.iold.EQ.0.AND.KINC.GT.3) THEN

c Restart if more than one contact pair opens (iold=0) in 2nd call
Iopen = Iopen + 1
C END IF

write(7,*) 'Status: Iopen=", Iopen, 'at int point", IINTP,
1 'in element", JELEM, 'and increment", KINC
write(7,*) 'IopenClose =",IopenClose,"iold=",iold,"KIT="", KIT

C Possible restart procedure
C IF (Iopen.gt.1) THEN
C write(7,"I") 'Too many contact openings: reduce increment'
C END IF
C END IF

C Restart if one contact pair closes (iold=1/IopenClose=0)
IF (IopenClose.EQ.0.AND.iold.EQ.1.AND.KINC.GE.2) THEN

iclose = iclose + 1
write(7,"I") 'Status: iclose=', iclose, 'at int point", IINTP,
1 'in element", JELEM, 'and increment", KINC

IF (iclose.gt.0) THEN
write(7,"I") 'Elastic unloading possible: reduce increment'
C END IF
C END IF

C Restart with PNEWDT (if PNEWDT less than 1)
C IF (iclose.gt.0) THEN
C PNEWDT = 1.0
ELSE IF (Iopen.gt.4) THEN
PNEWDT = 1.0
END IF

c  Sign definition (bridging stress acts as closure stress on structure
    c as in contact analysis)
    c Stiffness matrix according to ABAQUS definition: \(-dF/du!!\)

    stress(2) = - stress(2)

C  State variable update
    SVARS(IINTP) = iOpenClose

    return
end

C-----------------------------------------------------------------------------------------
subroutine KASET1(DMATRIX, IDIMX)
  INCLUDE 'ABA_PARAM.INC'
  PARAMETER (ZERO = 0.0D0)
  DIMENSION DMATRIX(IDIMX)

  DO i=1, IDIMX
    DMATRIX(i) = ZERO
  END DO

  RETURN
end

C-----------------------------------------------------------------------------------------
subroutine KASET2(DMATRIX, IDIMX, IDIMY)
  INCLUDE 'ABA_PARAM.INC'
  PARAMETER (ZERO = 0.0D0)
  DIMENSION DMATRIX(IDIMX, IDIMY)

  DO I=1, IDIMX
    DO J = 1, IDIMY
      DMATRIX(I,J) = ZERO
    END DO
  END DO

  RETURN
end
E Cohesive element verification

E.1 Verification of the 2D element

Figure 24 shows the simple two element model comparing the previously written subroutine UINTER [14] and the new user element. Both models give identical results. The nodes 1-6 for the user element initially coincide in their position: here they are plotted apart for a better understanding of the node numbering.

![Diagram showing 2D contact model and 2D user element verification](image)

*Figure 24. 2D user element model*

The input deck is simple and given in the following for the two-element problem. Note that the element is under plane stress condition (element, type=CPS8) to give comparable results to the 3D model with its boundary conditions.

```plaintext
*HEADING
*MATERIAL, name=mat1
*MAT, ENGINEERING CONSTANTS=1, 2
41.5E3, 9.5E3, 9.5E3, 0.3, 0.3, 0.3, 15.8E3, 3.65E3
3.65E3
```

---

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Figure 25 shows the results for the test case as created by ABAQUS/CAE. As the stiffness of the top element is large compared to the bridging stress input, the stress distribution in loading direction appears constant. The displacement field in x-direction, which is caused by the Poisson’s effect, varies linearly. For the simple case of uniform deformation (the same displacement boundary condition on all top nodes), the original bridging law is obtained from the reaction forces, element area and the opening of the element.
E.2 Verification of the 3D element

The input deck is simple and given in the following for the two element test analogous to the 2D model:

```plaintext
*HEADING
*NODE
1, 0.0, 0.0, 1.0
2, 1.0, 0.0, 1.0
3, 1.0, 0.0, 0.0
4, 0.0, 0.0, 0.0
5, 0.5, 0.0, 1.0
6, 1.0, 0.0, 0.5
7, 0.5, 0.0, 0.0
8, 0.0, 0.0, 0.5
9, 0.0, 0.0, 1.0
10, 1.0, 0.0, 1.0
11, 1.0, 0.0, 0.0
12, 0.0, 0.0, 0.0
13, 0.5, 0.0, 1.0
14, 1.0, 0.0, 0.5
15, 0.5, 0.0, 0.0
16, 0.0, 0.0, 0.5
17, 0.0, 1.0, 1.0
18, 1.0, 1.0, 1.0
19, 1.0, 1.0, 0.0
20, 0.0, 1.0, 0.0
21, 0.5, 1.0, 1.0
22, 1.0, 1.0, 0.5
23, 0.5, 1.0, 0.0
24, 0.0, 1.0, 0.5
25, 0.0, 0.5, 1.0
26, 1.0, 0.5, 1.0
27, 1.0, 0.5, 0.0
28, 0.0, 0.5, 0.0
*NSET, NSET=ALL
8, 10, 9
*NSET, NSET=BOTTOM
1, 2, 3, 4, 5, 6, 7, 8
*NSET, NSET=TOP
17, 18, 19, 20, 21, 22, 23, 24
*NSET, NSET=SIDEZ
1, 2, 5, 9, 10, 13, 17, 18, 21, 25, 26
*NSET, NSET=SIDEY
1, 1, 2, 3, 4, 5, 11, 12, 13, 14, 15, 16
*ELEMENT, TYPE=U16, ELSET=ALL
1, 2, 3
*ELEMENT, TYPE=U16, ELSET=BOTTOM
2, 9, 10, 11, 12, 13, 17, 18, 19, 20, 21, 22, 23, 24, 25, 26, 27, 28
*ELEMENT, TYPE=C3D20, ELSET=ALL
2, 9, 10, 11, 12, 13, 17, 18, 19, 20, 13, 14, 15, 16, 21, 22, 23, 24, 25, 26, 27, 28
*SOLID SECTION, ELSET=EALL, MATERIAL=MAT1, ORIENTATION=ORIENT1
*UEL PROPERTY, ELSET=ALL
1.65, 1.0, 0.5, 0.15, 1.0, 1000.0, 1.0, 3
1
*MATERIAL, name=mat1
*ELASTIC, TYPE=ENGINEERING CONSTANTS
41.5E3, 9.5E3, 9.5E3, 0.3, 0.3, 0.3, 15.8E3, 3.65E3
3.65E3
*ORIENTATION, name=orient1
1.0, 0.0, 0.0, 0.0, 1.0, 0.0
1, 0
*BOUNDARY
BOTTOM, 2, 0.0
SIDEY, 1, 0.0
SIDEZ, 3, 0.0
*STEP, INC=100, NLGEOM
```

The stress and displacement field are identical to the results presented for the 2D model in the previous appendix. For the simple case of uniform deformation (the same displacement boundary condition on all top nodes), the original bridging law is obtained by summing up all reaction forces on the top of the element and recording the opening of the element.

Figure 26. Verification of 3D user element
Mission

To promote an innovative and environmentally sustainable technological development within the areas of energy, industrial technology and bioproduction through research, innovation and advisory services.

Vision

Risø’s research shall extend the boundaries for the understanding of nature’s processes and interactions right down to the molecular nanoscale.

The results obtained shall set new trends for the development of sustainable technologies within the fields of energy, industrial technology and biotechnology.

The efforts made shall benefit Danish society and lead to the development of new multi-billion industries.