Cohesive zone modeling for structural integrity analysis of IC interconnects

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Received 28 February 2006; received in revised form 30 June 2006
Available online 10 October 2006

Abstract

Due to the miniaturization of integrated circuits, their thermo-mechanical reliability tends to become a truly critical design criterion. Especially the introduction of copper and low-\textit{k} dielectric materials cause some reliability problems. Numerical simulation tools can assist developers to meet this challenge. This paper considers the first bond integrity during wire bond qualification testing. During testing, metal peel off may occur. This mechanical failure mode is caused by delamination of several layers of the interconnect structure. An interfacial damage model is employed for simulating delamination. However, the fact that the considered interfaces are brittle triggers some reported numerical difficulties. This paper illustrates the potential of the interface damage mechanics approach for simulating metal peel off and it highlights the computational aspects to be developed to render a practically applicable approach.

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1. Introduction

The ongoing miniaturization of integrated circuits (ICs) requires the introduction of new materials in the interconnect structure, such as copper and low-\textit{k} dielectrics. These materials have substantially different mechanical properties compared with the conventionally used materials. In particular, the low-\textit{k} dielectric materials have a lower mechanical stiffness compared with the conventionally used SiO\textsubscript{2} and their adhesion to other materials is weaker [1,2]. Due to both the reduced feature sizes and the altered material properties, thermo-mechanical reliability tends to become one of the critical design criteria. Current design rules need to be improved to account for this compromised thermo-mechanical reliability. Proper interfacial damage models can assist IC developers to improve and optimize their design methods in this respect [3–6].

This paper focuses on the first bond integrity of wire bonded ICs. The wire bonds and the bond pads are subjected to critical loading conditions (possibly inducing damage) during different stages of the manufacturing process, for example in the bonding process, during qualification testing and during encapsulation. Here, the mechanical reliability of the bond pad during qualification testing is considered, more particularly during wire pull testing [7]. The wire bond passes this destructive test if the wire breaks in the heat affected zone above the ball at a specified strength. This is referred to as neck break. However, for some advanced ICs with fine pitch bond pads and with copper/low-\textit{k} interconnects a different failure mode is sometimes observed, in which the ball is lifted from the pad and part of the interconnect structure is torn from the IC [4,5,8–10]. This failure mode is referred to as metal peel off. Metal peel off is caused by the delamination of several layers in the interconnect structure. This failure mode is not allowed since the associated maximum load is usually below the qualification limit. Fig. 1 shows the result of several wire pull tests in which both failure modes can clearly be distinguished.

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Much effort is put into the development of methodologies to simulate and predict the interface delamination of the interconnect structure in an accurate manner. Several approaches have thereby been adopted, of which the three most relevant are:

- **Stress based approaches**
  Static linear elastic finite element (FE) simulations provide interface stresses [3] or first principal stresses [4]. Comparison with critical stress levels indicates the regions most prone to fracture. This approach assumes perfect adhesion between materials and therefore overestimates the strength of copper/low-k interconnects. To take poor adhesion or initial defects into account, another approach must be followed.

- **Linear elastic fracture mechanics (LEFM) based approaches**
  LEFM based approaches require the presence of initial defects. Explicit knowledge of the location and the size of an initial interface crack is thereby necessary. Furthermore, brittle fracture is assumed, as has indeed been observed for low-k dielectric films [2]. FE simulations provide the crack driving forces associated with the discrete interface cracks [3,6]. An interface delaminates if this crack driving force exceeds the fracture toughness of the considered interface. Furthermore, comparison of the crack driving forces in several interfaces provides a qualitative impression of the most vulnerable interfaces.

- **Area release energy approach**
  The major disadvantage of LEFM based approaches is that initial crack locations and sizes must be known. Recently, the use of the area release energy value as a fracture criterion has been proposed [5]. This quantity approximates for each node on the interface of interest the work of separation by releasing nodes adjacent to the considered node, as described a.o. in [11]. The difference with standard nodal release methods is that all nodes on the interface within a predefined distance from the considered node are released. This reduces the mesh dependency of the approximated energy values. The spatial distribution of the area energy release values shows where the critical areas for delamination are located.

None of the above approaches is able to simulate both the nucleation and growth of interface cracks in a unified manner. However, both aspects need to be considered to assess the first bond integrity, since initial cracks may not be present or their location may be unknown. Furthermore, cracks may be arrested and/or several cracks may evolve simultaneously. This situation is also not easily dealt with by the above methods. Therefore, this paper considers an interface damage mechanics approach, which takes the nucleation and evolution of interface cracks into account. It describes the gradual degradation of the adhesion between two materials using a cohesive zone model.

Cohesive zone modeling has proven its added value in many applications for ductile fracture [12–15] and for quasi-brittle fracture [16,17]. However, its application to brittle fracture still poses substantial computational challenges, which currently attract considerable attention [18–20]. In view of these research activities, this paper reports on the rather unexplored application of cohesive zone modeling for evaluating the mechanical reliability of IC interconnect structures. The objective here is to prove its feasibility for simulating interface delamination in copper/low-k interconnects, despite the numerical difficulties associated with brittle fracture. A simplified two-dimensional (2D) plane strain model of a characteristic part of the interconnect structure is constructed for this purpose. The load–displacement response of the model exhibits limit points, where the rate of the load or of the displacement switches sign. The existence of limit points requires a more sophisticated solution procedure than the commonly used load- or displacement-controlled Newton–Raphson procedure, e.g., the cylindrical arc-length method [21]. FE simulations reveal the nucleation and growth of several interface cracks. Furthermore, the simulations clearly indicate which aspects of the interface damage mechanics approach and the numerical solution procedure need to be developed further.

### 2. Cohesive zone model

Interface damage mechanics models the nucleation and growth of interface cracks. This approach does not require that the zone of influence near the crack tip is small compared with the crack size as in case of LEFM. Therefore, interface damage mechanics is applicable to ductile fracture processes as well [12–15]. A cohesive zone model describes the gradual degradation of the adhesion between two materials $\Omega_a$ and $\Omega_b$ along their common interface $\Gamma$. The non-linear response of the zone of influence near the crack tip is lumped into a plane for 3D problems or into a line for 2D problems. A traction–separation law provides the relation...
between the separation vector \( \delta \) (m) of the two material faces at an interface and the traction vector \( t \) (N/m²) acting between them. Fig. 2 explains graphically the lumping of the nonlinear material response in the zone of influence and it gives the orientation conventions for the separation \( \delta \) and the traction vector \( t \).

Numerous traction–separation laws have been proposed in literature, of which Chandra et al. [19] have provided an overview. These laws describe the nonlinear relations between the normal and shear components of the separation vector \( \delta = \delta_n \mathbf{n} + \delta_s \mathbf{s} \) and the traction vector \( t = t_n \mathbf{n} + t_s \mathbf{s} \), indicated by the subscripts \( n \) and \( s \). Several traction–separation laws are governed by a single relation between the effective separation \( \lambda \) (m) and the equivalent traction \( \tau \) (N/m²).

This paper considers only the Smith–Ferrante exponential type of traction–separation law given by [18]

\[
\tau = \tau_{\text{max}} \frac{\lambda}{\lambda_c} \exp \left(1 - \frac{\lambda}{\lambda_c}\right) \tag{6}
\]

and graphically represented in Fig. 3. The characteristic separation \( \lambda_c \) and the tensile strength \( \tau_{\text{max}} \) are the two independent constitutive parameters which describe this law. However, the work of separation per unit area \( G_c \) (J/m²) is commonly used as a constitutive parameter. It equals the area below the \( \tau-\lambda \) curve given by

\[
G_c = \int_0^\infty \tau(\lambda) \, d\lambda = \exp(1)\tau_{\text{max}}\lambda_c. \tag{7}
\]

where the expression for the equivalent traction \( \tau \) in Eq. (6) is used. The work of separation \( G_c \) is a measure for the adhesion energy, which can be determined by relatively simple experiments such as a four point bending test [1]. In case of laminates, the experimental determination of the tensile strength \( \tau_{\text{max}} \) or of the characteristic separation \( \lambda_c \) is more complicated. Using the work of separation \( G_c \) as model parameter, the traction–separation law can be expressed as

\[
\tau = \frac{G_c}{\lambda_c} \frac{\lambda}{\lambda_c} \exp \left(1 - \frac{\lambda}{\lambda_c}\right). \tag{8}
\]

Throughout this paper, the work of separation \( G_c \) and the characteristic separation \( \lambda_c \) are used as the two independent constitutive parameters and the tensile strength \( \tau_{\text{max}} \) is derived from them using Eq. (7).

Irreversible behavior is introduced by rewriting the traction–separation law in Eq. (8) in a typical damage mechanics form

\[
\tau = K(1 - D)\lambda \tag{9}
\]

with the virgin stiffness \( K \) (N/m³) defined as

\[
K = \frac{G_c}{\lambda_c}, \tag{10}
\]

which represents the initial stiffness in the undamaged case. In Eq. (9), \( D \) is the damage variable, which monotonically increases from 0 for the undamaged case to 1 for the completely damaged case. The following evolution law governs the damage:

\[
D = D(Q) = 1 - \exp \left(-\frac{Q}{\lambda_c}\right), \tag{11}
\]
where the separation history variable \( Q \) (m) satisfies the following Kuhn–Tucker conditions

\[
(Q - \lambda) \geq 0, \quad \dot{Q} \geq 0, \quad \dot{Q} \cdot (Q - \lambda) = 0.
\]

As a result of these relations, unloading follows the secant following Kuhn–Tucker conditions \( \dot{Q} \) where the separation history variable \( \lambda \) is redefined as follows:

\[
t_n = \frac{\delta_n}{\lambda} - K(-\delta_n).
\]

3. Implementation and computational requirements

The geometrically linear version of this cohesive zone model has been implemented in a 2D four-node user defined element in the commercial FE code MSC.MARC\textsuperscript{®}. Fig. 4 depicts the element conventions. The shear direction is from node 1 to node 2 in the undeformed configuration. The normal direction is perpendicular to the shear direction. Initially, the cohesive zone element is collapsed, i.e., it has a zero thickness. The nodal displacements form the element in the commercial FE code MSC.MARC\textsuperscript{®} model has been implemented in a 2D four-node user defined element in the commercial FE code MSC.MARC\textsuperscript{®}.

Consider a multi-DOF nonlinear system, which is governed by the following equilibrium equation:

\[
f_i(u) = f_e = \bar{f}_e.
\]

This relation indicates that the internal force vector \( f_i \), which is a nonlinear function of the solution vector \( u \) (or indeed a functional for the history-dependent behavior considered here), equals the external force vector \( f_e \). The external force vector \( f_e \) can be expressed as the product of a scalar load factor \( x \) and the unit force vector \( f_e \). In a load-controlled Newton–Raphson solution procedure, the external force is increased stepwise in each load increment by \( D\bar{x}\bar{f}_e \) and the corresponding equilibrium solution \( u \) is calculated iteratively. For each iteration \( i \) within a load increment, the following equation is solved:

\[
K_i(u^{(i-1)}) \frac{du^{(i)}}{du} = \bar{f}_e - f_i(u^{(i-1)})
\]

for the unknown iterative solution update \( du^{(i)} \). In Eq. (15), \( K_i(u^{(i-1)}) \) represents the tangential stiffness matrix of the previous iteration, defined as

\[
K_i(u^{(i-1)}) = \frac{\partial f_i}{\partial u} \bigg|_{u^{(i-1)}}.
\]

The updated solution \( u^{(i)} \) becomes

\[
u^{(i)} = u^{(i-1)} + du^{(i)} = u^{(i-1)} + Du^{(i)},
\]

where \( u^{(i)} \) represents the converged solution of the previous load increment and \( Du^{(i)} \) the incremental solution update. The update in Eq. (17) is repeated until \( u^{(i)} \) satisfies the equilibrium in Eq. (14) with a certain accuracy.

Fig. 5 illustrates the above iteration procedure graphically for an arbitrary DOF \( u \) from the solution vector \( u \).
Furthermore, this figure shows that the load-controlled procedure cannot pass the depicted snap-through point, since the scalar load factor $\alpha$ is fixed during the iteration process within each load increment. Fig. 5 clearly illustrates that for this fixed $\alpha_{n+1}$ in increment $n+1$ no solution exists. In order to pass the snap-through point, the scalar load factor $\alpha$ must be allowed to vary within a load increment. This is allowed as follows in a path-following technique [21]:

$$\alpha^{(i)} = \alpha^{(i-1)} + d\alpha^{(i)}$$

where $d\alpha^{(i)}$ represents the initially unknown iterative load factor update. The iterative solution update $d\alpha^{(i)}$ is subdivided in two parts as follows:

$$d\alpha^{(i)} = d\alpha^{(i)} + d\alpha^{(i)}d\alpha^{(i)}.$$  

The substitution in Eq. (15) results after some manipulation in the following two relations, which govern the iterative solution updates $d\alpha^{(i)}$ and $d\alpha^{(i)}$:

$$K_i(u^{(i-1)})d\alpha^{(i)} = \alpha^{(i-1)} F_c - F_i(u^{(i-1)}),$$

$$K_i(u^{(i-1)})d\alpha^{(i)} = \alpha^{(i)} F_c.$$  

An additional relation is required, which governs the iterative load factor update $d\alpha^{(i)}$ in Eq. (20). In case of the cylindrical arc-length method [21], this relation results from fixing the norm of the incremental solution update $Du^{(i)}$ to the arc-length $L$:

$$||Du^{(i)}|| = L.$$  

Fig. 6 shows how the cylindrical arc-length method is able to pass the previously considered snap-through point. It should be mentioned that a displacement-controlled Newton–Raphson procedure would be able to pass the snap-through point. However, a similar reasoning as above shows that such a control algorithm cannot pass a snap-back point. The cylindrical arc-length method is able to pass both types of limit points.

In the case of brittle fracture, the deformation tends to become more localized. The resulting force–displacement response may show sharp snap-back points, which render the convergence troublesome, even for the selected path-following technique (see a.o. [23,24]). The numerical simulations presented in this paper illustrate these convergence difficulties.

The advantage of being able to simulate both the nucleation and growth of interface cracks justifies additional efforts to overcome the above numerical difficulties of cohesive zone modeling. The numerical simulations reported in this paper show to what extent this can already be achieved and they indicate which aspects of the simulation tools require additional refinement.

5. Modeling of copper/low-$k$ interconnects below bond pads

The copper/low-$k$ interconnect structure below bond pads consists of several thin stacked layers of copper lines embedded in a low-$k$ dielectric material. Layer thicknesses are on the order of 300 nm. Also SiO$_2$ is used as an electrically insulating material in some layers in order to improve the mechanical reliability of the structure [3]. The use of copper as a conductor requires the deposition of very thin hardmasks (\(\approx 40\) nm thick), which are used as an etch stop during wafer fabrication. The layout of the interconnect structure, which we consider here, is characterized as follows (see Fig. 7):

- The structure consists of six vertically stacked copper lines (M1–M6), oriented alternately in $x$- and $y$-direction. The copper lines are connected vertically by pillars, the so called vias.
- A hardmask layer is placed above each layer of copper lines.
The dielectric material in the bottom and top layer is SiO$_2$ whereas a low-$k$ dielectric material is selected for the other layers.

In order to prove the feasibility of simulating the initial growth stages of interface cracks with cohesive zone modeling, only a small, yet characteristic part of the interconnect structure is considered, using a plane strain state assumption. Because of the latter simplification, quantitative predictions cannot yet be expected from this model. Nevertheless, as will be shown further on, the qualitative results well illustrate the capabilities of the adopted approach.

The copper, low-$k$ material, hardmask material and SiO$_2$ are linear elastic – Table 1 gives the relevant mechanical properties. Cohesive zone elements are placed along all material interfaces. Table 2 lists three sets of interface parameters used. The $\beta$-parameter is fixed arbitrarily to $\beta = 1/2$ in all cases. The polishing limit $[1]$ is selected as the work of separation per unit area $G_c$ for the copper/low-$k$ interfaces. Note that in reality, an ultra-thin barrier layer separates the copper and the dielectric material. This layer does not influence the overall stiffness of the structure significantly. However, the characteristics of the weak barrier/low-$k$ interface are selected as interface parameters for the fictitious copper/low-$k$ interface. The other interfaces have substantially higher values of $G_c$, which reflects their higher strength compared with the weak copper/low-$k$ interfaces.

The described model is subjected to the following set of boundary conditions (see Fig. 8).

- The bottom edge is fixed.
- The top edge is rigid and it is kept horizontal. The displacement of a control node on the top edge, indicated by the bullet in Fig. 8, is prescribed in a fixed direction given by the direction vector $\mathbf{v} = (v_x, v_z) = (1, 2)$.
- The arc-length control prescribes the load path of the force $F_z$ acting in the $z$-direction on the control node. Periodic boundary conditions are used for the opposite lateral edges.

Finally, two different FE meshes are considered, a coarse mesh, which is used as a reference, and a refined mesh. The refinement in the latter is such that each cohesive zone element is subdivided in two in the shear direction. The adjacent four-node plane strain elements are subdivided into four elements. The displacement continuity with the remaining plane strain elements is enforced by using linear constraint equations. In this manner, the mesh refinement is performed in the regions where the largest deformation gradients are expected, i.e., along the material interfaces. Fig. 9 shows a detail of the region near the layers M1 and M2 for the refined mesh.
6. Simulation of interface delamination

Fig. 10 shows the force–displacement response at the control node obtained with the coarse FE model and the interface data set 1, i.e., the set with the largest characteristic separations \( \lambda_c \) resulting in the most ductile response. The diagram shows the force \( F_z \) versus the adjoint displacement \( U_z \) and it exhibits several limit points where the rate of either \( F_z \) or \( U_z \) switches sign. Three characteristic points are indicated in the graph by a–c. Figs. 11–13 show the corresponding deformation patterns and the distribution of the \( \sigma_{zz} \) stress component in the copper lines and in the vias.

Only the region near the M1 and M2 layers is shown since delamination occurs mainly there. The obtained failure sequence can be explained as follows. The load transfer in the interconnect structure is from top to bottom. In each material layer, the stiff components, i.e., the copper lines and the copper vias, take care of most of the load transfer. This is reflected by the stress distribution, which is concentrated in the copper material in and surrounding the vias.

Only below the copper line M1 the vertical continuous chain of stiff components is interrupted by the lower hardmask and the interfaces 1 and 2 with this hardmask. The crack driving force in these interfaces is relatively high and hence these interfaces are vulnerable with respect to delamination. Also the weak interface 4 between the copper line M2 and the underlying low-\( k \) dielectric material is prone to delamination.
The following observations can be made for the three characteristic points in Fig. 10:

- Several interface cracks evolve simultaneously, for example the interfaces 1 and 2 with the bottom hardmask and the interface 4 between the copper line M2 and the underlying low-k dielectric material. At point a, which indicates the situation just after the maximum force is reached, a separation of the material faces at those interfaces can be observed. The interface 4 between the copper line M2 and the underlying low-k dielectric material is completely delaminated, i.e., the interface cannot transfer any tractions anymore. Subsequently, the stress distribution shifts the load transfer at that material layer mainly towards the copper via, for which the maximum stress level is reached at point a.

- At point a, significant stresses in the copper line M1 and in the lower hardmask occur primarily in the region under the copper via. As a result, the center regions of both the interfaces 1 and 2 are subjected to a large traction. Both the FE discretization and the interface properties are identical in these regions. However, since the central region of the upper interface is loaded slightly more, the tensile strength \( \tau_{\text{max}} \) is reached first there. The amount of elastically stored energy in the surrounding material is more than the energy required to completely separate the interface 2. Therefore, this interface delaminates instantaneously. Using proper settings of the arc-length control, the associated unstable force–displacement response between the points a and b is traced.

- At point b, the interface 2 between the copper line M1 and the lower hardmask is delaminated. Consequently, the lower hardmask and the interface 1 between the lower hardmask and the SiO\(_2\) are almost completely relaxed. The load transfer is now redirected through the intermetallic low-k dielectric material surrounding the copper line M1. Since the low-k dielectric material has a low mechanical stiffness, the corresponding overall stiffness of the interconnect structure drops substantially.

- Upon further loading, the interface 3, between the considered layer of low-k dielectric material and the hardmask on top of it, is separated. A crack propagates from the right to the left side of the interface 3. At the sharp snap-back point c, the crack reaches the left side.

- Point c indicates a sharp snap-back point. The interface 3 between the second hardmask and the intermetallic low-k dielectric material of layer M1 delaminates (see Fig. 13). The current mesh is too coarse in this region to model the resulting brittle fracture properly. Shortly after the snap-back point, the solution procedure follows the global elastic unloading path for which all elements in the model unload elastically. Proper control of the load path would allow the solution procedure to proceed further until complete delamination of the interface 3 occurs.

The results obtained with the refined FE model, still using the interface data set 1, are similar to those of the coarse model. Fig. 14 compares both force–displacement responses of the control node. Differences occur only after complete delamination of interface 2, as indicated by point b. Due to the mesh refinement, the crack along interface 3 grows further (see Fig. 15). However, the solution procedure still follows the global elastic unloading path prior to the complete delamination of interface 3.

For the cohesive zone parameters used so far, the numerical results illustrate that the interface damage mechanics approach is well capable of simulating the simultaneous evolution of several interface cracks in a multi-layered structure. However, when the structure has lost most of its structural integrity, the solution procedure breaks down in the sense that all elements unload elasti-
cally. Mesh refinement postpones this critical point for the considered parameter settings.

Next, the effect of reducing the characteristic separation $\lambda_c$ is investigated. Recall that with the other model parameters fixed, an overall more brittle response is expected, which likely presents more and sharper snap-back points in the force–displacement response (see Section 3). Figs. 16 and 17 show the force–displacement responses for the three interface data sets in Table 2. The corresponding deformation patterns and stress distributions are not provided, because they do not differ substantially from those presented for interface data set 1. The following remarks can be made:

- The force–displacement response shows indeed a more brittle behavior for smaller $\lambda_c$ values. The first limit point becomes a sharp snap-back point. Unfortunately, the cylindrical arc-length control breaks down earlier in the sense that it follows the global elastic unloading path already soon after passing the first limit point. This computational deficiency is discussed further on.

- The initial overall stiffness of the interconnect structure is higher for smaller $\lambda_c$ values. This results from the higher values for the virgin stiffness $K$ of the cohesive zones according to Eq. (10), which equals the initial stiffness in the undamaged case.

- The strength of the interconnect structure is higher for smaller $\lambda_c$ values. This can be explained as follows. Recall that the stress distributions do not differ substantially for the different interface data sets. The load is still primarily transferred through the stiff vertical chain of copper material in and surrounding the vias. Furthermore, the critical interfaces with the lower hardmask become stronger, i.e., their tensile strength $\tau_{\text{max}}$ is higher for a smaller $\lambda_c$ value. Consequently, by increasing the strength of the weakest link, the strength of the interconnect structure increases.

- Mesh refinement postpones the breakdown of the solution procedure for interface data set 1 only. For the other sets, the global elastic unloading path is followed in an earlier stage in which the mesh requirements are not yet fulfilled. This seems contradictory but it is in fact caused by the choice of the nonlinear solution procedure, as will be explained in the discussion below.

The above simulations prove the feasibility of the cohesive zone modeling. In case of the considered plane strain models, the simulations predict failure near the copper lines M1 and M2. The evolution of the stress distribution gives insight in the load transfer. This knowledge is very useful for future interconnect layout optimization in which for example the effect of the via distribution is taken into account [5].

7. Discussion

Comparison of the simulation results for three interface data sets and for two FE models shows that the current
implementation exhibits increasing numerical difficulties as the characteristic separation $\lambda_c$ is diminished. According to the literature [20], mesh refinement should suffice to deal with the corresponding increased brittleness, but the current simulations do not confirm this. The solution procedure breaks down in an earlier stage in case of mesh refinement. The reason for this contradiction resides in the selection of the cylindrical arc-length control as the nonlinear solution procedure. In this path-following technique, the norm of the incremental update of the solution vector $\|Du^i\|$ is prescribed. All DOFs have an equal weight in the determination of the external load update. This is not the most efficient procedure for the strong deformation localization considered here. The efficiency increases if more weight is given to the DOFs associated with the interface delamination. However, if the mesh refinement is applied as discussed in Section 5, the relative number of DOFs in the bulk part of the model increases more than the relative number of cohesive zone DOFs. As a result, the cylindrical arc-length method breaks down in an early stage if the level of mesh refinement becomes too high.

Our results clearly indicate that solely satisfying the mesh requirements is not sufficient anymore in case the cohesive zone modeling is applied to brittle interfaces. An enhanced solution control procedure is necessary as well. Future investigations will therefore focus on the use of local subplane methods or weighted subplane methods [23,24] for the current application. These methods allow one to give more (or even all) weight to the DOFs which are most sensitive to deformation localization. Moreover, the external load can be controlled by carefully selected functions of the DOFs. For example, the separations $\delta$ of the cohesive zone elements or the damage $D$ in those elements may be selected as a control function. If a proper solution procedure is developed and used, the mesh requirements may probably be relaxed. Future investigations should provide knowledge on the most appropriate remeshing schemes in direct relation to the most appropriate control functions in this sense.

8. Conclusions

This paper presents the ability of an interface damage mechanics approach in simulating delamination in copper/low-$k$ IC interconnect structures. This approach allows one to consider several simultaneously evolving cracks, which were not present initially and whose location is a priori unknown. The adopted cohesive zone model describes the gradual degradation of adhesion between two materials along their common interface. A linear version of the cohesive zone model due to Smith and Ferrante has been implemented in a 2D user defined element in the commercial FE code Msc.Marc, which is applied in a plane strain FE analysis. The cylindrical arc-length control is selected as the nonlinear solution procedure in order to trace the complex force-displacement response due to the deformation localization which is inherent to brittle fracture.

The use of a cohesive zone model for brittle interfacial fracture, observed in copper/low-$k$ interconnects, poses some numerical difficulties, which are not present in the common use of cohesive zones in ductile materials or interfaces. The deformation tends to become more localized in the brittle case and the associated force-displacement response generally exhibits – sometimes very sharp – snap-back points. In order to comply with the mesh requirements for cohesive zone modeling, the FE discretizations may become too fine from a practical point of view. Furthermore, this paper shows that a standard method to deal with snap-back points, the adopted cylindrical arc-length method, breaks even down in case of mesh refinement. Nevertheless, the advantage of being able to simulate both the nucleation of growth of interface cracks triggers the need for adequate solutions to overcome these intrinsic numerical difficulties. The results of the feasibility study, reported here, illustrate that this is already achieved to some extent but that there is still room for further improvement.

Future implementations should use a carefully selected local subplane-control method instead of the (global) cylindrical arc-length method. This allows one to give more weight to the DOFs which are most sensitive to deformation localization, or to include functions which depend directly on the damage in the cohesive zone elements. If a proper solution procedure is developed, the mesh requirements might be relaxed. With these improvements, the performance of 3D cohesive zone models can be assessed, which are necessary for more realistic and reliable simulations of metal peel off. Future work will address this topic in more detail.

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