Fracture of Underfilled and Edge-Bonded BGA-PCB Assemblies:  
Effects of Joint Geometry, Strain Rate and Stiffness

By

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A thesis submitted in conformity with the requirements for the degree of Doctor of Philosophy  
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Abstract

The reliability of the solder joints connecting microelectronic components to printed circuit boards (PCBs) is a serious concern in the design of many electronic devices. The focus of the present research was the fracture performance of component-solder-PCB assemblies with and without underfill adhesives under bending loading conditions.

Fracture tests of copper–solder–copper double-cantilever-beam (DCB) specimens with a single continuous solder layer showed that for joints shorter than a characteristic length the fracture load increased with increasing solder joint length. Also, the fracture tests of DCBs with two discrete solder joints showed the strength was maximum at an optimal joint spacing. An analytical solution was developed for the characteristic length of mode I DCB specimens with a single continuous solder layer as well as the optimal spacing of double-joint DCBs, taking into account the mechanical properties and dimensions of the substrates and solder.

Fracture experiments with underfilled ball grid array (BGA)- PCB specimens in a DCB configuration showed that the specimens always failed in the PCB. The strength of the underfilled BGAs was solely a function of the fillet size, and independent of the underfill thermal and mechanical properties.
Edge-bonding is a less expensive alternative to underfilling, in which only the edges or corners of the component are bonded to the PCB using a viscous adhesive. Edge-bonded and underfilled specimens with fillets of the same size and shape tested using DCB specimens failed at the same load, because the stress distribution in the PCB near the PCB-fillet interface was only a function of the adhesive fillet size and shape, and independent of the extent of the adhesive layer between the PCB and the BGA.

Finally, a cohesive zone model (CZM) was developed to predict the PCB delamination bending loads in underfilled BGA-PCB assemblies. The cohesive parameters were obtained from fracture tests of bending test specimens consisting of PCB substrates bonded with the underfill adhesive. The model provided relatively accurate predictions of the failure mode and fracture load of the underfilled BGA-PCB assemblies for a range of PCB stiffnesses, adhesive fillet sizes, and strain rates.
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Chapter 1

1. Introduction

1.1. Overview and motivation

Fracture of solder joints is one of the main reasons for the failure of surface-mount microelectronic assemblies. Solder joints are used either in the form of closely-spaced, multiple joints, such as ball grid array (BGA) packages, where load sharing occurs, or as relatively isolated single joints such as in heat sinks or power modules. These solder joints range in thickness from approximately 100 μm to less than 500 μm, and in length from approximately 100 μm to several centimeters [1-2]. In spite of the wide range of solder joint lengths used in microelectronics devices, there is a gap in the literature regarding the effect of joint length on joint strength.

Under mechanical loading, systems of multiple solder joints often experience an uneven load distribution resulting in the inefficient over-loading of one of the joints, typically at the periphery of a device [3-7]. The maximum strength is achieved when there is a reasonably uniform load distribution between the joints. In order to predict load sharing in multiple solder joints, it is of interest to develop an analytical model that takes into account the effects of substrate stiffness and joint spacing on the load carried by each solder joint.

There is a large literature on the mechanical reliability of solder balls connecting BGA packages to printed circuit boards (PCBs). The BGA is a common approach to surface-mount packaging technology, in which the whole bottom surface of the package is covered with a grid of solder bumps. In reflow soldering, the entire BGA-PCB assembly is heated to melt the solder and
permanently connect the package to the PCB. A common technique to reinforce these solder balls is underfilling, in which a low viscosity epoxy adhesive is dispensed along the edges of the BGA and flows into the gap beneath the BGA and between the solder balls through the capillary action. It is then thermally cured to form an underfilled BGA-PCB assembly (Fig. 1.1). The cured adhesive enhances the mechanical reliability of the board level solder balls under bending, vibration and drop loadings [8-11].

Fig. 1.1. Configuration of an underfilled BGA-PCB assembly.

An underfilled BGA-PCB assembly is a practical example of an adhesive joint. Small variations in the local geometry at the end of the overlap region of an adhesive joint (i.e. the adhesive spew fillet) can significantly influence its mechanical strength [12-13]. Several studies [10-11] have used three-point and four-point bending test specimens to investigate the reliability of underfilled microelectronic components. However, the effect of the size and shape of the underfill fillet on the bending strength of BGA-PCB assemblies has not been quantified and isolated from the effects of adhesive mechanical and thermal properties.

In many BGA applications underfilling the entire area between the component and the PCB is not required. To reduce the costs associated with underfilling, edge-bonding was introduced, in which a more viscous adhesive is applied only on the corners or edges of the component [14-16].
Compared with underfilled components, edge-bonded components are also easier to replace and inspect [17].

The previous studies on the mechanical strength of edge-bonded and underfilled BGA-PCB assemblies have reported conflicting results. For example, Shi and Ueda [18] argued that under drop-impact loading adhesives with high modulus have better performance than the adhesives with low modulus. However, Chang et al. [19] compared the effect of a silica-filled underfill epoxy and an unfilled underfill epoxy with different properties on the drop reliability of BGA-PCB assemblies and found both underfills gave similar results. In addition, no reported studies have explained the measured performance differences in terms of the stress states that give rise to failure along the observed crack paths. There remains a need for a systematic study of the effects of underfill and edge-bond adhesive properties, fillet shape, and the curing-induced residual stress on BGA-PCB strength.

A critical reliability issue in microelectronic assemblies is delamination at the interfaces between the layers of dissimilar materials in the component–solder–PCB sandwich as a result of PCB or substrate bending during board assembly, shipment, handling, and end use [20-22]. Interlaminar damage or delamination is one of the main failure modes in PCB composite laminates, because they are much weaker out-of-plane than in-plane. Multilayer PCBs are composite laminates consisting of conducting copper layers and woven glass-fiber epoxy insulating layers bonded together under heat and pressure. Cohesive zone models (CZMs) are increasingly used to simulate crack initiation and propagation in composite laminates [23-25]. It is therefore of interest to develop a CZM to predict the initiation and growth of delamination in underfilled BGA-PCB assemblies under different bending conditions.
1.2. Objectives

The main objectives of the Ph.D. research were to:

1) assess the dependence of solder joint fracture strength on the joint length.

2) investigate the effect of substrate properties and joint spacing on load sharing in multiple solder joints.

3) characterize the crack path and failure pattern in underfilled BGA-PCB assemblies under bending loading conditions.

4) compare the bending performance of underfilled and edge-bonded BGA-PCB assemblies, and assess the effect of key manufacturing parameters.

5) develop a general method to predict the fracture load of underfilled BGA-PCB assemblies, and to evaluate the proposed method by comparing fracture strength predictions with experimental measurements for a variety of underfilled BGA-PCB test vehicles loaded over a range of strain rates.

1.3. Thesis outline

Chapter 2 presents an analytical substrate–solder–substrate sandwich model to investigate the variation of the maximum peel stress of the solder layer with the joint length. In this model the solder layer was regarded as a continuous distribution of tensile and shear springs connecting the substrates. This work has been published as:


and a portion of the results were in the proceedings of the international conference:

Akbari S, Nourani A, Spelt JK. Bending strength of solder joints as a function of joint length. SMTA International (SMTAi), Sep 2015, Rosemont, IL, USA.
Chapter 3 explains the effect of joint spacing and substrate properties on load sharing in multiple solder joints using a discrete foundation model, in which each solder joint was modeled with one or two tension-compression springs. This work has been published as:


and some of the results were in the proceedings of the international conference:

*Akbari S, Nourani A, Spelt JK. A discrete foundation model to predict load sharing in pairs of solder joints. International Conference on Soldering and Reliability (ICSR), May 2016, Toronto, ON, Canada.*

Chapter 4 assesses the effect of the size of the spew fillet of the underfill adhesive on the bending strength of underfilled BGA-PCB assemblies. Three different adhesives with a broad range of thermal and mechanical properties were considered. This work has been published as:


and some of the results were in the proceedings of the international conference:

*Akbari S, Nourani A, Spelt JK. Effect of viscosity and fillet size of board-level underfills on reliability of BGA assemblies. International Conference on Soldering and Reliability (ICSR), May 2015, Toronto, ON, Canada.*

Chapter 5 compares the bending strength of underfilled and edge-bonded BGA-PCB assemblies. Two low viscosity adhesives were used for underfilling, while two high viscosity adhesives were considered for edge-bonding. This work has been published as:
Chapter 6 explains the development of a cohesive zone model (CZM) to predict delamination initiation and propagation in multilayer PCBs assembled with soldered BGAs that were then reinforced with an underfill epoxy adhesive. The model was used to predict the crack paths and the bending strength of underfilled BGA-PCB assemblies for a range of adhesive fillet sizes, PCB stiffnesses, and strain rates. This work has been submitted for publication as:


and a portion of the results were in the proceedings of the international conference:

Akbari S, Nourani A, Spelt JK. Spelt. Cohesive zone modeling of failure in underfilled BGA-PCB assemblies under bending. SMTA International (SMTAi), Sep 2016, Rosemont, IL, USA.

Finally, Chapter 7 reviews the main conclusions and contributions of this research, and recommends future work.
1.4. References


Chapter 2

2. Effect of solder joint length on fracture under bending

2.1. Introduction

Solder joints in electronic and microelectronic devices are used to join various substrates having different bending stiffnesses. For example, they are used to join chips and substrates in first level packaging, or to connect components and printed circuit boards (PCBs) in board-level packaging [1-2]. Most solder joints in microelectronics assemblies are used either in the form of multiple, closely-spaced joints, such as ball grid arrays, where load sharing occurs, or as relatively isolated single joints such as in power modules or heat sinks or resistors [3-4]. These joints range in length from approximately 100 µm to several centimeters, and in thickness from approximately 100 µm to less than 500 µm [4-5]. In spite of the wide range of solder joint lengths in common microelectronics applications, there is a gap in the literature regarding the effect of joint length on joint strength.

A number of studies have investigated the effect of the thickness and length of solder layers on the strength of copper-solder-copper tensile specimens [6-9] as shown in Fig. 2.1. For example, Yin et al. [7] (Fig. 2.1-a) used joints between copper wires to assess the effect of the thickness and diameter of a Sn3.0Ag0.5Cu (SAC305) solder layer. For thicknesses of 75 - 525 µm and diameters of 200 -575 µm it was shown that a decrease in the thickness or diameter induced higher triaxial stresses in the solder layer resulting in increases up to approximately 100% in the ultimate tensile strength.
Tests aimed at predicting the strength of solder joints in a particular application must be capable of reflecting the nature of the actual loading and the resulting stress states. The mechanical failure of most solder joints results from PCB or substrate bending; therefore, tensile test specimens such as those in Fig. 2.1 do not represent the dominant stress states found in many practical loading conditions.

A large amount of research has been directed towards obtaining stresses in sandwich-type structures such as adhesively-bonded joints using beam bending analyses [10-14]. The analytical model presented by Goland and Reissner [10] idealized the substrates in an adhesively-bonded joint as cylindrically bent plates. Bigwood and Crocombe [12] developed a linear elastic analysis to determine the adhesive peel and shear stresses in the overlap region. Their approach was attractive in its generality, since the adherend-adhesive-adherend sandwich was isolated as a free-body from arbitrary joint configurations and subject to the loads imposed by the bounding adherends.

![Fig. 2.1. Copper-solder-copper tensile specimens used by (a) Yin [7] (b) Zimprich [8] (c) Cugnoni [9]](image-url)
The major objective of the present study was to evaluate the effect of solder joint length on the fracture strength and strain energy release rate at crack initiation, $G_{ci}$. Experiments were conducted using the bending-type test specimen, the double cantilever beam (DCB), to measure the mode-I fracture strength of solder joints of various lengths. These fracture strengths were then used in a linear elastic finite element analysis (FEA) to calculate $G_{ci}$, which was then evaluated as a failure criterion for these joints of varying length. The sandwich stress model proposed by Bigwood and Crocombe [12] was modified to consider the effect of the thickness of the solder layer as well as the shear deformation of the substrates. The strength data were then compared with the peel stress predictions of the analytical substrate-solder-substrate sandwich model and the finite element model under the assumption that solder fracture is dominated by substrate peel deformation.

### 2.2. Experimental procedures

Mode I fracture experiments were conducted on copper-solder-copper DCB specimens made with a continuous solder joint of different lengths (Fig. 2.2) prepared using typical industry values for the time-above-liquidus and cooling rate as described in [15]. The solder was SAC305 with a thickness of 127 μm, and the arms of the DCB were made with copper bars (C110 alloy) of square cross-section (12.7 ×12.7 mm).

The copper bars were first milled to create sharp edges and were then polished using an ultra-fine silicon carbide/nylon mesh abrasive to produce a repeatable surface roughness of $R_a=1 \, \mu m$, which is very similar to that on the copper pads of commercial PCBs. The copper bars were then immersed in acetone and wiped with cheese cloth. The copper surfaces were masked with Kapton tape to produce specimens having six different lengths (2, 5, 10, 15, 30, and 50 mm). The Kapton tape formed a smooth square edge at the end of the solder layer; however, it has been shown that
crack initiation in solder joints in DCBs is largely independent of the local geometry of the end of the solder layer [15].

Fig. 2.2. Schematic of the copper-solder-copper DCB specimen.

All dimensions in mm. Not to scale.

Two 127 μm diameter steel wires were used between the copper bars to control the thickness of the joints. After heating the copper bars on a hot plate, a flux-cored SAC305 solder wire (Kester Inc., USA) was applied to the copper surfaces to be soldered and the joint was closed and left for 120s (i.e. the time-above-liquidus) before being transferred to a small wind tunnel to achieve a cooling rate of 1.4-1.6 °C/s. Excess solder that flowed from the edges of the joint was wiped away immediately, so no machining of the sides of the DCB was required before testing. The loading-pin holes were drilled in the arms of the DCB. Fracture experiments were conducted at a constant cross-head speed of 4.23 mm/s, corresponding to an opening strain rate of 0.22 s⁻¹ at the end of the solder layer, as calculated using a finite element model. Five specimens were tested for each joint length. All samples were fracture tested one day after fabrication.
2.3. A general substrate-solder sandwich model

Following the approach of [12] for adhesive joints, the solder stress analysis was based on a substrate-solder-substrate sandwich (Fig. 2.3-a) representing a free-body of the entire solder region of an arbitrary joint. Although the present case was the DCB of Fig. 2.2, for generality the two substrates have thicknesses \( h_1 \) and \( h_2 \) and the solder layer has thickness \( t \) and joint length \( L \).

![Diagram of a substrate-solder-substrate sandwich](image)

**Fig. 2.3.** a) A two-dimensional substrate-solder-substrate sandwich element under general loading. The first index refers to the substrate and the second index to left or right end of the sandwich. b) A free-body diagram of an element of the sandwich over a differential length \( dx \).

The sandwich is isolated from the surrounding structure of the joint as a free-body and the reactions in each substrate are determined. Since such sandwiches can be isolated in any planar
joint including solder joints in heat sinks, resistors and power modulus, the approach is quite general, and is applicable to a large range of joint configurations.

Linear elastic constitutive behavior was assumed for the substrates and solder. Because the thickness of the solder layer was much smaller than the thickness of the substrates, the variation of the stresses and strains across the width of the solder layer (z-direction) was neglected in the analytical model, and the solder stresses were considered to be uniform through the thickness (y-direction). To verify the analytical results, the peel stresses were also calculated using a finite element model at the mid-plane of the solder layer, as discussed in Section 2.4.

2.3.1. Formulation of the problem: Timoshenko beam theory

Force and moment equilibrium of the representative element (Fig. 2.3-b) was used to relate the moment, normal and shear forces of the substrates to the shear and peel stresses in the solder layer:

\[
\frac{dN_1}{dx} = -\tau, \quad \frac{dN_2}{dx} = \tau \tag{1}
\]

\[
\frac{dV_1}{dx} = -\sigma, \quad \frac{dV_2}{dx} = \sigma \tag{2}
\]

\[
\frac{dM_1}{dx} = V_1 - \tau \frac{h_1 + t}{2}, \quad \frac{dM_2}{dx} = V_2 - \tau \frac{h_2 + t}{2} \tag{3}
\]

The subscripts 1 and 2 denote the upper and the lower substrates, respectively. These equations account for the thickness of the solder layer, unlike the original development of the sandwich model by Bigwood and Crocombe [12] where it was considered to be negligible in adhesive joints.

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Bigwood and Crocombe [12] modeled the substrates 1 and 2 in the overlap region as Euler-Bernoulli beams, thereby neglecting transverse shear deformation. However, in short stubby beams transverse shear deformations can be significant, and were therefore considered in the present analysis by using Timoshenko beam theory. Accordingly,

\[ u(x, y) = u(x, 0) + y \frac{\partial u(x, y)}{\partial y} = u_0 + y \phi(x) \]  \hspace{1cm} (4)

\[ v(x) = v(x, 0) = v_0 \]  \hspace{1cm} (5)

where \( u \) and \( v \) are the beam displacements in the \( x \) and \( y \) directions, respectively, \( \phi(x) \) is the beam rotation, and \( u_0 \) and \( v_0 \) are the displacements of a point on the mid-plane (\( y = 0 \)). The axial and shear strains are thus given by

\[ \varepsilon_x = \frac{du_0}{dx} + y \frac{d\phi}{dx} \]  \hspace{1cm} (6)

\[ \gamma_{xy} = \frac{dv_0}{dx} + \phi \] \hspace{1cm} (7)

The internal forces in the substrates are then

\[ N = A \frac{du_0}{dx} \] \hspace{1cm} (8)

\[ V = C \left( \frac{dv_0}{dx} + \phi \right) \] \hspace{1cm} (9)

\[ M = D \frac{d\phi}{dx} \] \hspace{1cm} (10)
where $A$, $C$ and $D$ are the extensional, shear, and bending stiffnesses of the Timoshenko beam, respectively, defined as:

$$
A = \frac{hE}{1-\nu^2}, \quad C = khG, \quad D = \frac{Eh^3}{12(1-\nu^2)}
$$  \hfill (11)

with $E$, $G$ and $\nu$ being the Young’s modulus, shear modulus and Poisson’s ratio of the respective beams, and $k$ is the shear correction factor, equal to 5/6 for a Timoshenko beam with rectangular cross section [16]. Equations (8-10) can be simplified as:

$$
\frac{d^2v_0}{dx^2} = \frac{1}{C} \frac{dV}{dx} - \frac{M}{D}
$$  \hfill (12)

$$
\frac{du_0}{dx} = \frac{N}{A}
$$  \hfill (13)

$$
\frac{d\phi}{dx} = \frac{M}{D}
$$  \hfill (14)

Substituting Eqs. (13-14) into Eq. (6), gives the axial strains at the upper and lower solder-substrate interfaces (Fig. 2.3a):

$$
\varepsilon_{x,1} = \frac{N_1}{A_1} - \frac{h_1M_1}{2D_1}
$$  \hfill (15)

$$
\varepsilon_{x,2} = \frac{N_2}{A_2} + \frac{h_2M_2}{2D_2}
$$  \hfill (16)
2.3.2. Governing differential equations

In cases where the solder layer has a much lower axial and bending stiffness than the substrates, such as in the present DCB, the solder layer can be modeled as a normal and shear spring, and the solder strains follow from the relative displacements of the substrates [17]. According to the kinematics of Timoshenko beam theory, given by Eqs. (4-5), transverse beam displacements are assumed constant throughout the beam thickness, while the axial beam displacements vary linearly through the thickness. Solder shear and peel stresses can thus be expressed as:

\[
\sigma = E_s^* \varepsilon_s = E_s^* \frac{V_{0.2} - V_{0.1}}{t}, \quad E_s^* = \frac{E_s}{1 - \nu_s^2} \tag{17}
\]

\[
\tau = G_s \gamma_s = \frac{G_s}{t} \left( u_{0.2} - u_{0.1} - \frac{h}{2} (\phi_2 + \phi_1) \right) \tag{18}
\]

where \(E_s, G_s, \nu_s\) are the Young’s modulus, shear modulus and Poisson’s ratio of the solder, and \(E_s^*\) is the Young’s modulus of the solder under the present plane strain conditions.

Differentiating Eq. (17) four times and substituting Eqs. (1-3), (12), (15) and (16), leads to the first differential equation relating solder peel and shear stresses:

\[
\frac{d^4 \sigma}{dx^4} + \eta_1 \frac{d^2 \sigma}{dx^2} + \eta_2 \sigma = \eta_3 \frac{d \tau}{dx} \tag{19}
\]

\[
\eta_1 = -\frac{E_s^*}{t} \left( \frac{1}{C_1} + \frac{1}{C_2} \right) \tag{20}
\]
The second differential equation relating the solder peel and shear stresses is obtained by differentiating Eq. (18) three times and substituting Eqs. (2-3) and (12):

\[ \eta_2 = \frac{E_s}{t} \left( \frac{1}{D_1} + \frac{1}{D_2} \right) \]  
\[ \eta_3 = \frac{E_s}{t} \left( \frac{h_2 + t - h_1 + t}{2D_2 - 2D_1} \right) \]  

(21)

(22)

In general, the full solution of Eqs. (19) and (23) requires a numerical method; however, in the case of identical substrates:

\[ h_1 = h_2 = h, \quad A_1 = A_2 = A, \quad C_1 = C_2 = C, \quad D_1 = D_2 = D \]  

(26)

The solder peel and shear stresses are thus decoupled leading to:

\[ \eta_2 \frac{d^4 \sigma}{dx^4} + \eta_3 \frac{d^2 \sigma}{dx^2} + \eta_2 \sigma = 0 \]  
\[ \frac{d^3 \tau}{dx^3} + \eta_1 \frac{d \tau}{dx} = 0 \]  

(27)

(28)

Equations (27) and (28) have the following solutions:
\[ \sigma(x) = m_1 \sinh(r_{01}x) + m_2 \cosh(r_{01}x) + m_3 \sinh(r_{02}x) + m_4 \cosh(r_{02}x) \]  

(29)

\[ r_{01,02} = \frac{1}{\sqrt{2}} \sqrt{-\eta_1 \pm \sqrt{\eta_1^2 - 4\eta_2}}, \quad \eta_1 = -\frac{2E_s^*}{tC}, \quad \eta_2 = \frac{2E_s^*}{tD} \]  

(30)

\[ \tau(x) = m_5 + m_6 \sinh(r_{01}x) + m_7 \cosh(r_{01}x) \]  

(31)

\[ r_{03} = \sqrt{-\xi}, \quad \xi = -\frac{2G_s}{t} \left( \frac{1}{A} + \frac{h(h+t)}{4D} \right) \]  

(32)

2.3.3. Boundary conditions

The seven boundary conditions used to find the constants in Eqs. (29) and (31) are given below. The first four boundary conditions are obtained by relating the applied forces and moments at either end of the sandwich (Fig. 2.3) to the relevant derivatives of the peel stress equation, Eq. (29):

\[ \frac{t}{E_s^*} \frac{d^2 \sigma(0)}{dx^2} = \left( \frac{1}{C_2} + \frac{1}{C_1} \right) \sigma(0) + \frac{M_{11}}{D_1} - \frac{M_{21}}{D_2} \]  

(33)

\[ \frac{t}{E_s^*} \frac{d^2 \sigma(L)}{dx^2} = \left( \frac{1}{C_2} + \frac{1}{C_1} \right) \sigma(L) + \frac{M_{12}}{D_1} \frac{M_{22}}{D_2} \]  

(34)

\[ \frac{t}{E_s^*} \frac{d^3 \sigma(0)}{dx^3} = \left( \frac{1}{C_1} + \frac{1}{C_2} \right) \frac{d\sigma(0)}{dx} + \frac{V_{11}}{D_1} - \frac{V_{21}}{D_2} \]  

(35)

\[ \frac{t}{E_s^*} \frac{d^3 \sigma(L)}{dx^3} = \left( \frac{1}{C_1} + \frac{1}{C_2} \right) \frac{d\sigma(L)}{dx} + \frac{V_{12}}{D_1} - \frac{V_{22}}{D_2} \]  

(36)
The next two boundary conditions are obtained by relating the applied forces and moments at either end of the sandwich to the relevant derivatives of the shear stress equation, Eq. (31):

\[
\frac{t}{G_s} \frac{d\tau(0)}{dx} = \left( \frac{N_{21}}{A_2} - \frac{N_{11}}{A_1} \right) - \left( \frac{h_2M_{21}}{2D_2} + \frac{h_1M_{11}}{2D_1} \right) \tag{37}
\]

\[
\frac{t}{G_s} \frac{d\tau(L)}{dx} = \left( \frac{N_{22}}{A_2} - \frac{N_{21}}{A_1} \right) - \left( \frac{h_2M_{22}}{2D_2} + \frac{h_1M_{21}}{2D_1} \right) \tag{38}
\]

The last boundary condition is obtained from the horizontal equilibrium of the upper substrate.

\[
\int_0^L \tau \, dx = N_{11} - N_{12} \tag{39}
\]

### 2.3.4. Simplified analysis: Euler-Bernoulli beam theory

Equations (27-28) for the solder peel and shear stresses can be simplified if it is assumed that the substrates can be modelled using ordinary beam theory (Euler–Bernoulli beam theory) in which transverse shear deformation is neglected, so that

\[
\gamma_{xy} = 0 \quad \rightarrow \quad \frac{\partial u}{\partial y} = -\frac{\partial v}{\partial x} = \phi \tag{40}
\]

and Eq. (4) becomes:

\[
u(x, y) = u_0(x) - y \frac{\partial v}{\partial x} \tag{41}
\]

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Euler–Bernoulli beam theory assumes that the shear stiffnesses of the substrates, \( C_1 \) and \( C_2 \), are very large, so that all terms containing \( 1/C_1 \) and \( 1/C_2 \) in Eqs. (33-36) disappear and Eqs. (27-28) are simplified as:

\[
\begin{align*}
\frac{d^4 \sigma}{dx^4} + \eta_2 \sigma &= 0 \\
\frac{d^3 \tau}{dx^3} + \frac{\varepsilon_1}{\eta_1} \frac{d \tau}{dx} &= 0
\end{align*}
\] (42)

\[
\sigma = n_1 \cos(s_{01}x) \cosh(s_{01}x) + n_2 \cos(s_{01}x) \sinh(s_{01}x) + n_3 \sin(s_{01}x) \cosh(s_{01}x) + n_4 \sin(s_{01}x) \sinh(s_{01}x)
\] (43)

\[
\tau(x) = n_5 + n_6 \sinh(s_{02}x) + n_7 \cosh(s_{02}x)
\] (44)

\[
s_{01} = \sqrt[4]{\eta_2 / 4} = \sqrt{\frac{E_1}{2 t D}}, \quad s_{02} = \sqrt{-\varepsilon_1}
\] (45)

This is the formulation developed by Bigwood and Crocomb [12] for adhesive joints. The seven boundary conditions used to find the constants \( n_1 \) to \( n_7 \) for identical substrates are presented in Appendix 2A.

**2.3.5. Mode I DCB joint with identical substrates**

The loads applied to the present DCB test specimen (Fig. 2.2) were

\[
\begin{align*}
N_{11} &= N_{12} = N_{21} = N_{22} = 0 \\
V_{11} &= -V_{21} = F, \quad V_{12} = V_{22} = 0 \\
M_{11} &= -M_{21} = F \bar{a}, \quad M_{12} = M_{22} = 0
\end{align*}
\] (46)
The maximum opening stress based on Timoshenko beam theory, $\sigma_{\text{max,TM}}$, occurs at the left edge of solder layer and is given by Eq. (29):

$$
\sigma_{\text{max,TM}} = \sigma(0) = \frac{F\eta_2}{2r_{02}^2} \left[ 2 \sinh(r_{01}L/2) \sinh(r_{02}L/2) + ar_{02} \sinh[L/2(r_{01} + r_{02})] \right] \\
+ \frac{F\eta_2}{2r_{01}^2} \left[ 2 \cosh(r_{01}L/2) \cosh(r_{02}L/2) + ar_{01} \sinh[L/2(r_{01} + r_{02})] \right]
$$

(47)

The maximum opening stress based on Euler-Bernoulli beam theory, $\sigma_{\text{max,EB}}$, is given by Eq. (43) with the constants of Appendix 2A:

$$
\sigma_{\text{max,EB}} = \sigma(0) = n_1 = b_1 R_6 / R_5 + b_3 R_3 / R_5
$$

(48)

From Eq. (48), it can be seen that for joints longer than a specific value, such that $s_{01}L \geq 3$, the maximum opening stress converges to a constant value, independent of the joint length, and $\sigma_{\text{max,EB}}$ can be approximated as

$$
s_{01}L \geq 3 \rightarrow R_3 / R_5 \approx 1, \quad R_6 / R_5 \approx 1 \rightarrow \sigma_{\text{max,EB}}^{\text{appr}} = b_1 + b_3
$$

(49)

Therefore, the Euler-Bernoulli model can be used to provide the following convenient closed-form approximation of the characteristic length, $L_{cr}$, for a mode I DCB solder joint:

$$
s_{01}L = 3 \rightarrow L_{cr} = \left( \frac{13.5tEh^3}{E_s(1-v^2)} \right)^{0.25}
$$

(50)

Mode I DCB joints longer than $L_{cr}$ have a maximum opening stress that no longer increases with joint length. Equation (50) shows that the characteristic length is a function of the thickness...
and mechanical properties of the solder and substrates. For a given solder layer, joints with stiffer substrates (i.e. larger cross-section or with a greater Young’s modulus) have a larger $L_{cr}$, because the opening stress is distributed over a longer distance extending from the end of the solder layer. Therefore, a large $L_{cr}$ implies a reduced stress concentration at the end of the solder joint. If it is assumed that the opening (peel or mode I) stress governs fracture initiation and the strength of a solder joint, joints that are longer than $L_{cr}$ will have strengths that are independent of the joint length. Section 2.5 will compare the predictions of $L_{cr}$ made using the various models with the present experimental results.

2.4. Finite element model

A finite element analysis of the DCB joint of Fig. 2.2 was used to evaluate the Timoshenko and Euler-Bernoulli sandwich models. Two-dimensional, 8-node quadratic elements (Plane183, ANSYS®15, Ansys Inc, Canonsburg, PA) were used to model the solder layer and copper bars. Considering the dimensions of the solder layer and the copper bars in the DCB joints tested, plane strain and plane stress conditions were assumed for the solder layer and the copper bars respectively (Fig. 2.4). However, in the analytical model presented in Section 2.3, plane strain conditions were assumed for both the solder layer and the substrates. It will be shown in the following sections that this difference had a negligible effect on the solder stresses.

As will be shown in Section 2.5, the load-displacement curve of the fracture experiments was completely linear until final fracture, so plastic deformation was negligible. Therefore, both the copper bars and the solder were modeled as isotropic, linear-elastic materials with the material properties of Table 2.1.

To provide confidence in the accuracy of the two-dimensional model, the solder stresses were also calculated using a three-dimensional model with eight-node solid elements (ANSYS
type Solid45). The effect of the solder plasticity on the calculation of the characteristic length was investigated using an elastic-perfectly plastic model of the solder layer.

At least 15 elements were used through the thickness of the solder layer to capture the steep stress gradients, especially in the end regions. Mesh independence was established by re-meshing with smaller elements. The rate of displacement of the ends of the specimen at the location of loading pins was used to calculate the local rate of change of the solder layer thickness and the opening strain rate of the solder layer, which was 0.22 s\(^{-1}\) in all of the tests. Over this range of strain rates, the Young’s modulus of the solder could be assumed to be independent of the strain rate [18]. The fracture load was used in the FE model (FEM) to calculate stress distribution in the mid-plane of the solder layer.

Fig. 2.4. FEM of the DCB test specimen used to calculate stresses in solder layer.
Table 2.1. Mechanical properties used in the FE analysis [15, 18].

<table>
<thead>
<tr>
<th>Material</th>
<th>Young’s modulus (GPa)</th>
<th>Poisson’s ratio</th>
</tr>
</thead>
<tbody>
<tr>
<td>Cu (110 alloy)</td>
<td>124</td>
<td>0.35</td>
</tr>
<tr>
<td>SAC305 (Sn3.0Ag0.5Cu)</td>
<td>41</td>
<td>0.4</td>
</tr>
</tbody>
</table>

The finite element model was also modified to calculate the critical strain energy release rate at crack initiation, $G_{ci}$, using the measured fracture load for different joint lengths. $G_{ci}$ was calculated using the $J$-integral method [19-20] assuming a 250 μm long crack in the mid-plane of the solder layer. In order to capture the $r^{-1/2}$ singularity near the crack tip, the region near the crack tip was meshed with singular elements [21].

The energy release rate was calculated along five different paths around the crack tip to check the path independence of the $J$-integral as illustrated in Fig. 2.5 for three paths. The values obtained from these five paths were almost identical.

Fig. 2.5. The $J$-integral contours surrounding the crack tip within the solder layer.
2.5. Results and discussion

2.5.1. Solder peel stress distributions

Figures 2.6 and 2.7 compare the various model predictions of the peel stress along the mid-plane of the solder layer of mode-I DCB specimens with solder lengths of 50 mm and 5 mm, respectively. Since the primary interest was in the distribution of the peel stress rather than its magnitude, the force applied to both arms of the DCB was arbitrarily set as \( F = 200 \) N, which was smaller than the measured failure force for the 5 mm joint (presented below). In the 50 mm long joint (Fig. 2.6), the peel stress was maximum at the left edge of the joint, decreased within a short distance to become compressive, and then increased to zero. The sandwich model based on Timoshenko beam theory was closer to the FE predictions, being better able to approximate both the stress distribution and the maximum peel stress. The differences were caused by the Timoshenko assumption that transverse shear strain changes in a linear manner through the thickness of a beam. Higher-order beam theories, which allow the transverse shear strains to vary nonlinearly through thickness of a beam, are more accurate [22-24]. The most noticeable difference between the analytical models and the FEM was the location of the transition between tensile and compressive peel stress in the 50 mm long joint (Fig. 2.6b); i.e. about 2 mm and 6 mm, respectively. This difference was much smaller for the 5 mm joint (Fig. 2.7), where all three models predicted a tensile peel zone of approximately 2.5 mm (i.e. half the joint length). Also, Fig. 2.6b shows that the predictions of the three-dimensional model fit the two-dimensional data very well, thereby supporting the applicability of the two-dimensional approximation.
Fig. 2.6. Solder peel stress predictions along the mid-plane of a 50 mm long mode I DCB joint with load $F=200$ N: (a) peel stress distribution along the entire solder layer and (b) peel stresses near the left edge of the solder layer.
Fig. 2.7. Solder peel stress predictions along the mid-plane of a 5 mm long mode I DCB joint with load $F=200$ N.

The strain rate used in the present experiments was $0.22$ s$^{-1}$, where the yield strength of SAC305 solder is $S_y=60$ MPa [18]. Since a linear elastic finite element was used, the maximum von Mises stress at the solder layer of a copper-solder-copper DCB specimen could be larger than the yield strength. However, the purpose of the peel stress calculations was to find the characteristic length, $L_{cr}$, from the changes in the stress distribution along the joint; hence, the absolute values of the stresses were not of interest. To investigate the effect of solder plasticity on $L_{cr}$, the solder layer was modeled as an elastic-perfectly plastic material. As with the elastic model, $L_{cr}$ was assumed to be the joint length where the maximum peel stress reached a constant value. It was noted that the calculation of $L_{cr}$ was unaffected by consideration of solder plasticity, and the present results were identical to those obtained using an elastic-plastic finite element model.

In order to find the characteristic length, $L_{cr}$, of the DCB of Fig. 2.2, the maximum solder peel stress was plotted versus the joint length using an arbitrary constant applied force, as shown
in Fig. 2.8 for \( F=200 \) N. Since a linear elastic model was used to determine these stress distributions, the joint characteristic length where the maximum peel stress becomes constant (\( L_{cr} \)) was independent of the applied load. As the joint becomes shorter, it is seen that the maximum peel stress increases rapidly and the differences between the analytical models and the FEM grow. In particular, the Euler-Bernoulli predictions become increasingly unreliable since they neglect transverse shear deformation of the beams which becomes more important in short joints. The maximum peel stress predictions of the Timoshenko model are quite close to that of the FEM, and more importantly, the prediction of \( L_{cr} \) is essentially the same; i.e. approximately 15 mm.

**Fig. 2.8.** Maximum predicted solder peel stress at the mid-plane of solder layer for different joint lengths and \( F=200 \) N.

Accounting for shear deformation becomes more important when the substrates have a relatively low transverse stiffness, such as in a printed circuit board (PCB) which has a layered
structure that is much weaker and more flexible out-of-plane than in-plane [25]. In these cases, the Euler-Bernoulli beam theory will become less accurate than the Timoshenko model.

2.5.2. Experimental results and discussion

Figure 2.9 shows a typical load–displacement response for a DCB fracture test of a 30 mm long joint, with data points recorded at a sampling rate of 1 kHz in order to accurately capture the maximum load. The initial nonlinearity in the curve was due to the take-up of the clearance in the pin used to load the DCB arm.

In DCB fracture tests of adhesive and solder joints, the onset of nonlinearity in the load-displacement curve is considered a measure of adhesive or solder plastic deformation [26-27]. Figure 2.9 illustrates that the load-displacement curves in the present experiments were completely linear until final fracture, so plastic deformation and R-curve crack-tip toughening were negligible [15]. Similar behavior was observed in all of the tests, regardless of joint length. Therefore, the maximum measured force was considered to coincide with both crack initiation and final fracture. These maximum loads were used in FE models of Section 2.4 to calculate stress distributions in the solder layer. The absence of significant R-curve behavior contrasts with the toughening reported by Nadimpalli and Spelt [15] using SAC305 solder and similar specimens when tested under quasi-static conditions (strain rate of $6 \times 10^{-5}$ s$^{-1}$). This difference was due to the much higher strain rate used in the present experiments (0.22 s$^{-1}$), which caused the joint to undergo fast fracture before a crack tip damage zone could develop and grow [15].
Figure 2.10 shows the measured fracture loads for the DCB specimens of different solder layer lengths. As expected from the maximum peel stress predictions of Fig. 2.8, for relatively short solder joints the fracture load increased with joint length up to approximately a joint length of 13-15 mm, after which it remained constant. The $t$-test showed that the difference between the average fracture load values for relatively long joints (15, 30, and 50 mm) was statistically insignificant at the 95% confidence level. Therefore, based on the measured joint strengths, $L_{cr}$ was approximately 15 mm, which agrees well with the FEM and Timoshenko model predictions of Fig. 2.8.

According to the sandwich model based on the more approximate Euler-Bernoulli beam theory, Eq. (50), the characteristic length of this DCB specimen was $L_{cr}$ = 11 mm. The underestimate of the actual beginning of the fracture strength plateau was due to the neglect of the transverse shear deformation of the substrates in the Euler-Bernoulli formulation.
An important practical implication of these results is that for each joint configuration under various loads, a characteristic length can be defined based on the change of the maximum peel stress and the corresponding fracture strength as a function of the joint length. The use of joints longer than the characteristic length provides no additional strength to the joint. For simple geometries such as the mode I DCB, the characteristic length can be found analytically, but for more complex cases, a numerical solution like the FEM must be used. Nevertheless, the approximate closed-form expression for $L_{cr}$, given by Eq. (50), illustrates the general effects of changes in beam and solder stiffness.

2.5.3. **Solder joint fracture load prediction**

The critical strain energy release rate at crack initiation, $G_{ci}$, corresponding to the measured fracture loads for different joint lengths was calculated using the FEM and the $J$-integral method of Section 2.4. Figure 2.11 shows that the effect of the joint length on the $G_{ci}$ was negligible, with
differences being statistically insignificant ($t$-test, 95% confidence level). There was slightly more scatter as the joint length became smaller, because the $G_{ci}$ calculation was more sensitive to the error in the fracture load for shorter joints.

In the present context of treating $G_{ci}$ as a solder joint property for the purpose of strength prediction, a solder joint system is comprised of a specific solder, solder reflow profile, and substrate surface finish; i.e. the material and process parameters that can influence fracture are fixed. This implies that the crack path and the microstructure of the fracture surfaces of joints of various shapes and sizes should be the same provided that the mode ratio of the loading is the same. As will be discussed in Section 2.5.4, the SEM and EDX analyses confirmed this for the present experiments. The dominant failure mode was crack propagation close to the interface between the solder layer and the intermetallic compound (IMC) for all solder joints. It is noted that the stiffness and dimensions of the joint and the substrates are not defined as part of a solder system since they do not affect $G_{ci}$. This was established by the fracture measurements of Nadimpalli and Spelt [28] using mode I and mixed-mode DCB specimens with various solder and substrate thicknesses. They concluded that $G_{ci}$ was a property for a given solder joint system and could be used as a fracture criterion.

To evaluate $G_{ci}$ as a solder joint failure criterion using the present data, the fracture loads for the short joints (2, 5, 10, 15, and 30 mm) were predicted using the average $G_{ci}$ obtained from the fracture loads for the longest joint (50 mm) representing a traditional DCB fracture specimen. Table 2.2 shows that the predictions of the $G_{ci}$ method agreed reasonably well with experiments, having a maximum error less than 10%. However, the fracture load predictions were consistently smaller than the measured joint strengths, simply because $G_{ci}$ of the 50 mm joint was slightly
smaller than for the other lengths (Fig. 2.11), although these differences in $G_{ci}$ were statistically insignificant.

**Fig. 2.11.** Effect of joint length, $L$, on the $G_{ci}$ of mode I copper–solder–copper DCB joints.

Error bars indicate ±1 standard deviation based on 5 measurements at each length.

<table>
<thead>
<tr>
<th>Joint length, $L$ (mm)</th>
<th>Measured fracture load (N)</th>
<th>Predicted fracture load (N)</th>
<th>% difference (N)</th>
</tr>
</thead>
<tbody>
<tr>
<td>2</td>
<td>59.0</td>
<td>55.0</td>
<td>-7</td>
</tr>
<tr>
<td>5</td>
<td>251</td>
<td>246</td>
<td>-2</td>
</tr>
<tr>
<td>10</td>
<td>723</td>
<td>659</td>
<td>-9</td>
</tr>
<tr>
<td>15</td>
<td>1030</td>
<td>963</td>
<td>-7</td>
</tr>
<tr>
<td>30</td>
<td>1190</td>
<td>1120</td>
<td>-6</td>
</tr>
</tbody>
</table>

Table 2.2. Comparison of measured average solder fracture loads with predictions based on average measured $G_{ci}=880$ J/m$^2$ for 50 mm long joint in the mode I DCB of Fig. 2.2.
In summary, the data presented support the $G_{cl}$ fracture criterion as a promising approach to predict the fracture load of solder joints with varying joint length. The ability of this approach to predict the fracture load of very small joints in microelectronics devices such as BGAs needs further investigation, and is the subject of an ongoing study. However, this approach is still directly applicable to the prediction of the strength of longer joints such as those found in heat sinks and power electronics.

2.5.4. Fracture surface analysis

In all specimens, the crack grew quickly along one interface. Fig. 2.12 (a) shows the typical fracture surface of the tested specimens. It shows that the fracture surface was smooth and macroscopically flat, which is a characteristic of solder fracture surface at intermediate and high strain rates [29-31].

The microstructure of the fracture surfaces was inspected using scanning electron microscopy (SEM). The elemental analysis was performed using the energy-dispersive X-ray spectroscopy (EDX) during the SEM process. Inspections of the solder fracture surfaces displayed a mixture of both brittle and ductile features. For example, the brittle fracture of Cu$_6$Sn$_5$ grains leaves flat hexagonal features on the fracture surface (Fig. 2.12 (b)), as confirmed by the EDX spectrum (note the Cu peak in Fig. 2.12 (d)). Besides, the areas of relatively uniform, fine-grained roughness are typical of brittle decohesion along the solder-IMC interface [15, 29-31]. These brittle failure modes in general indicate the initial failure [32].

Also, ductile fracture through the solder layer creates dimples with round shapes (Fig. 2.12 (b)), which is confirmed with an EDX spectrum (Fig. 2.12 (c)). This ductile fracture is a result of post-failure crack propagations [32].
These results suggest that the crack propagated close to the more highly strained substrate (the upper substrate in Figs. 2.2 and 2.4) above the interface between the solder and IMC layer. It is worth noting no distinct difference was observed between the fracture surfaces of joints with various lengths.

**Fig. 2.12.** (a) A typical fracture surface. Crack direction is from left to right. (b) SEM micrograph of the fracture surface, (c-d) EDX spectrum of the fracture surface.
2.5.5. Case study: chip resistor

The concept of a characteristic joint length is applicable to the solder joints used in surface-mount microelectronics assemblies. For example, Fig. 2.13 illustrates a chip resistor with solder joints of length \( L_2 \) at either end. When the PCB is subject to bending, the resulting peel stresses in the solder layer can cause solder cracking. For a typical chip resistor with the material properties and dimensions of Table 2.3, a finite element model was used to determine the maximum peel stress in the solder layer using the boundary conditions and applied load, \( F \), of Fig. 2.13. The distance between the loading point and the edge of the chip, \( L_F \), was assumed to be 4.88 mm.

![Fig. 2.13. Schematic of a chip resistor assembly.](image)

Table 2.3. Dimensions and material properties of a typical chip resistor [33]

<table>
<thead>
<tr>
<th>Layers</th>
<th>( h ) (mm)</th>
<th>( L ) (mm)</th>
<th>( E ) (GPa)</th>
<th>( \nu )</th>
</tr>
</thead>
<tbody>
<tr>
<td>PCB (1)</td>
<td>1.23</td>
<td>13</td>
<td>22</td>
<td>0.28</td>
</tr>
<tr>
<td>Solder (2)</td>
<td>0.12</td>
<td>Variable (0.3-3)</td>
<td>41</td>
<td>0.4</td>
</tr>
<tr>
<td>Resistor (3)</td>
<td>0.65</td>
<td>6.5</td>
<td>131</td>
<td>0.3</td>
</tr>
</tbody>
</table>

For the bending configuration of Fig. 2.13 the maximum peel stress in the right-hand joint occurred at the right-most edge of the solder layer. Figure 2.14 shows this maximum peel stress as a function of the solder joint length, \( L_2 \). It can be postulated that the characteristic length of this
resistor solder joint under the given bending load is approximately 1 mm, meaning that joints longer than 1 mm are not stronger than a 1 mm long joint.

![Graph](image_url)

**Fig. 2.14.** FEM predictions of the maximum peel stress as a function of the length of the right-hand solder joint in Fig. 2.13. Stress calculated at the mid-plane on the right side of the joint. Properties of Table 2.3 with applied load $F=2$ N/mm.

### 2.6. Conclusions

Fracture tests on mode-I copper DCB specimens with SAC305 solder joints of various lengths showed that the fracture load increased with solder joint length before reaching a plateau value of constant joint strength. The critical strain energy release rate for solder joint fracture, $G_{ci}$, was, however, independent of the joint length, supporting its use as a fracture property of the solder joint system.
These results were then compared with the solder stresses predicted by two analytical substrate-solder-substrate sandwich models of the joined region. In these models, the solder layer was effectively regarded as a continuous distribution of linear-elastic tensile and shear springs connecting the substrates. It was assumed that the dimensions of the joint were sufficient to permit a homogeneous, continuum model of the solder. The strains in the copper substrates were related to the applied loads using either the Euler-Bernoulli beam theory, which did not account for shear deformations, or the Timoshenko beam theory which assumed that transverse shear strain varied linearly through the thickness of the substrate. The model results were verified by finite element analysis.

Both models showed that the maximum peel stress became independent of the length of the solder layer beyond a characteristic length that was a function of the thickness and the mechanical properties of the solder layer and the substrates. The main advantage of Euler-Bernoulli analysis over Timoshenko analysis was its ability to give an explicit expression for the characteristic length of simple geometries such as mode I DCB joints. The characteristic length was a function of the bending stiffnesses of the substrates and the solder layer. The characteristic length predicted by the models was very close to the solder length corresponding to the onset of the joint-strength plateau measured in the DCB specimens, lending further support to the models.

It was also shown that the critical strain energy release rate for solder joint fracture, \( G_{ci} \), is independent of the joint length. An important practical implication of this finding is that the \( G_{ci} \) from a long DCB fracture specimen can be used to predict the fracture of much shorter joints, such as those found in microelectronics assemblies. Although the present model assumed that the solder properties were isotropic and homogeneous, the treatment of very small solder joints may require a statistical distribution of anisotropic properties.
The concept of the characteristic length was illustrated in a chip resistor assembly under PCB bending. The characteristic length of a typical chip resistor solder joint was predicted to be about 1 mm, as determined from the finite element predictions of the maximum solder opening stress as a function of the joint length.

Appendix 2A. Constants of Euler-Bernoulli solution for identical substrates

The four constants in Eq. (43) for the peel stress distribution were found using the boundary conditions Eqs. (33-36) neglecting terms containing $1/C_1$ and $1/C_2$:

$$n_1 = \left[ b_3 R_3 - 2b_2 \sinh(s_{0,1}L) \sin(s_{0,1}L) + b_1 R_6 + b_4 R_4 \right] / R_5$$

$$n_2 = \left[ b_2 R_2 - b_3 \sinh^2(s_{0,1}L) - b_4 \sinh(s_{0,1}L) \sin(s_{0,1}L) \right] / R_5$$

$$n_3 = \left[ b_2 R_2 - b_3 \sinh^2(s_{0,1}L) - b_4 \sinh(s_{0,1}L) \sin(s_{0,1}L) \right] / R_5$$

$$n_4 = b_1$$

$$b_1 = s_{01}^2 (M_{21} - M_{11})$$

$$b_2 = s_{01}^2 (M_{22} - M_{12})$$

$$b_3 = s_{01} (V_{21} - V_{11})$$

$$b_4 = s_{01} (V_{22} - V_{12})$$

$$R_1 = \cosh(s_{0,1}L) \sin(s_{0,1}L) - \sinh(s_{0,1}L) \cos(s_{0,1}L)$$

$$R_2 = \cosh(s_{0,1}L) \sin(s_{0,1}L) + \sinh(s_{0,1}L) \cos(s_{0,1}L)$$

$$R_3 = \cosh(s_{0,1}L) \sinh(s_{0,1}L) - \cos(s_{0,1}L) \sin(s_{0,1}L)$$

$$R_4 = \cosh(s_{0,1}L) \sinh(s_{0,1}L) + \cos(s_{0,1}L) \sin(s_{0,1}L)$$

$$R_5 = \sinh^2(s_{0,1}L) - \sin^2(s_{0,1}L)$$

$$R_6 = \cosh^2(s_{0,1}L) - \cos^2(s_{0,1}L)$$

Similarly, Eqs. (37-39) were solved to find the constants of the shear stress equation, Eq. (44):
\[ n_5 = \frac{b_2 - b_1 \cosh(s_{0,2}L)}{s_{0,2} \sinh(s_{0,2}L)} \]

\[ n_6 = \frac{b_1}{s_{0,2}} \]

\[ n_7 = \frac{b_3}{L} - \frac{(b_2 - b_1)}{s_{0,2}^2} \]

(2A.4)
2.7. References


Chapter 3

3. Load sharing between discrete solder joints in bending: effect of spacing and joint properties

3.1. Introduction

A typical microelectronics assembly has a sandwich-type structure consisting of surface mount devices attached to a printed circuit board (PCB) using solder joints which serve as mechanical connections as well as conductors of electricity and heat. Surface mount assemblies may contain a single, continuous solder joint (e.g. light emitting diodes [1-3], and power electronics modules [4-6]), two discrete solder joints as illustrated in Fig. 3.1a (e.g. capacitors [7], chip resistors [8], and heat sinks [9]), or an array of tens of solder joints as in Fig. 3.1b (e.g. ball grid array packages [10, 11], and quad flat packages [12, 13]). The mechanical reliability of solder joints is an essential consideration in microelectronics design, and much attention has been paid to their fatigue and fracture properties [14-18].

There is a large literature on the reliability of solder joints in multi-joint configurations such as BGAs and resistors. This literature can be divided into two broad categories: papers dealing with thermal stresses and those treating stresses due to various forms of mechanical loading such as shock, vibration, and quasi-static shear or bending. Analytical models have been developed to predict thermal stresses in neighboring solder and adhesive joints. For example, Suhir [19] presented an analytical model to determine thermal stresses due to mismatch of material properties in a bimaterial assembly, adhesively bonded at the ends and subjected to temperature change.
However, this model predicted large interfacial shear stresses at the free ends of the assembly, and so did not satisfy the stress-free boundary condition. A more accurate model of the interfacial thermal stresses in the solder joints of a leadless chip resistor accounted for the interaction of the local and global thermal mismatches, and was able to satisfy the zero shear stress condition at the free ends of the joints [8]. Finite element modelling has been used to analyze the mechanical reliability of BGA solder joints under impact [20, 21], bending [22, 23], and vibration loading [24, 25]. All these studies have concluded that the outermost solder ball is always under the maximum stress, and carries the maximum share of the load. However, the complexity of the loading has precluded attempts to develop analytical models and generalize the effects of substrate stiffness and joint spacing on the load carried by each solder ball.

The mechanical failure of solder joints in surface-mount microelectronic assemblies results mostly from the PCB bending during board assembly, handling, shipment, and end use [26-29] as illustrated in Fig. 3.1. Systems of multiple solder joints often experience an uneven load distribution leading to the inefficient over-loading of one of the joints, typically at the periphery of a device. The maximum strength improvements are achieved when there is a uniform load sharing among the joints. It is therefore of interest to be able to predict load sharing in multiple solder joints.

The present study examined the effects of joint geometry and mechanical properties on load sharing and the optimal spacing between two solder joints in various bending configurations using both experiments and modeling. The models are evaluated in various bending geometries and compared with experimental data.
3.2. Experimental procedures

Copper-solder-copper DCB specimens were fabricated with two 2 mm long discrete solder joints (Fig. 3.2a) having different gaps between them (d = 2, 5, 10, 20, 40, 60 and 80 mm). The solder joints were made with Sn3.0Ag0.5Cu (SAC305) with a thickness of 127 μm, controlled using steel wires. The spacing wires were removed from the double joint specimens after fabrication so that they did not influence the load sharing between the joints. The copper bars (C110 alloy) were cut and milled to the dimensions of Fig. 3.2 and then polished using an ultra-fine silicon carbide/nylon mesh abrasive pad on an orbital sander to produce a surface roughness of $R_a=1$ μm. This surface condition was very similar to that on the copper pads of PCBs with an organic solderability preservative (OSP) surface finish commonly used in microelectronics [30]. The copper bars were then wiped with acetone to remove contaminants and covered with Kapton tape to exclude the surfaces to be soldered and form a smooth square edge at the end of each solder layer.

After heating the copper bars on a hot plate to 220–225 ºC, a flux-cored SAC305 solder wire (Kester Inc., USA) was touched to the copper surfaces to be soldered. The temperature of the joint was monitored using a thermocouple inserted in the copper bar very close to the solder layer. The
two copper bars were then clamped against the two 127 μm diameter steel wires and the
temperature was maintained to establish the desired time-above-liquidus (TAL) of 120 s. The
specimen was then transferred to a small wind tunnel to cool to room temperature at a rate of 1.4–
1.6 °C/s. Both the TAL and the cooling rate were typical of solder joints in commercial surface
mount devices. More details of the fabrication process can be found in [30-32].

![Fig. 3.2. Schematic of the copper-solder-copper DCB specimens used to evaluate models in
a specific bending configuration. (a) a double joint with joint spacing $d$ used in this study.
(b) the single joint specimen used in [31]. All dimensions in mm. Not to scale.](image)

After soldering, 6.5 mm diameter loading-pin holes were drilled in the DCB arms. The
specimen loading pins were connected to a servo-electrical testing machine, and the specimens
were loaded in mode I until the crack initiated in the solder layer. The loading rate was 4.23 mm/s,
corresponding to a peel strain rate of 0.22 s$^{-1}$ at the opening end of the solder layer, as calculated
using a finite element model. Five specimens were tested for each joint spacing.
These procedures and materials were the same as used in ref. [31] for the fabrication of the single-joint DCB specimens of Fig. 3.2(b) having continuous solder lengths $L = 2, 5, 10, 15, 30,$ and $50$ mm.

### 3.3. Analytical models

Beam-on-elastic-foundation models have been used to calculate stresses and strain energy release rates in adhesively-bonded joints as illustrated schematically in Fig. 3.3. The approach of Bigwood and Crocombe [33], originally developed for the stress analysis of the adhesively-bonded joints, was used to model the DCB specimen of Fig. 3.2b as two beams connected with a continuous distribution of springs, as shown in Fig. 3.3a. This continuous foundation model, described in Section 3.3.1, provided the peel stress distribution in the solder layer of Fig. 3.2b. In this model, the foundation represents only the solder layer, and the substrate properties are contained in the coupled beam analysis of each arm of the DCB. A modified form of the model, described in Section 3.3.2, was also used for the stress analysis of the DCB double joints of Fig. 3.2a.

A second beam-on-elastic-foundation model was developed for adhesively-bonded DCB specimens by Penado [34], following the work of Kanninen in which the arms of a cracked homogeneous DCB were modelled as if each provided a foundation for the other [35]. Using this model, the deformed shape of the upper substrate in the DCB specimen of Fig. 3.2b was modeled as a beam supported by a foundation with a modulus having contributions from the solder layer and the opposing substrate, as illustrated in Fig. 3.3b. A discrete form of this model is described in Section 3.3.3 to investigate the effect of different parameters on load sharing in assemblies with two solder joints, e.g. Fig. 3.2a.
Fig. 3.3. Beam-on-elastic-foundation models of a DCB joint: a) Bigwood and Crocombe approach [33], where the foundation represents the compliance of the bonding layer,  

b) Penado approach [34], where the foundation represents the compliance of the bonding layer and the lower substrate.

3.3.1. Continuous foundation model of a single solder joint

The continuous foundation model treats each solder joint as a continuous distribution of shear and tensile springs connecting the substrates. Following the approach used by Bigwood and Crocombe for adhesive joints [33], the single joint of Fig. 3.2(b), consisting of two relatively stiff substrates of the same material and thickness $h$, bonded by a thin layer of solder of length $L$ and thickness $t$, was modeled using the continuous foundation model (CFM) of Fig. 3.4. Crack initiation was mixed-mode under this generalized loading which can represent any combination of in-plane tension, shear and bending loads that might be applied to a two-dimensional solder joint.

The deformations were described in terms of a local coordinate system with the origin at the mid-plane of the left edge of each substrate bounding the solder joint. The subscripts 1 and 2 in Fig. 3.4(a) refer to the loads applied to the upper and lower substrates, respectively. The superscript $L$ refers to the load applied to the left end of the sandwich corresponding to $x=0$, while the superscript $R$ refers to the load applied to right end of the sandwich at $x=L$. 

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Fig. 3.4. Continuous foundation model for stress analysis of a DCB joint. a) A substrate-
solder-substrate sandwich element with a single joint under a generalized loading.

b) Free-body diagrams of differential elements of substrates and solder.

The substrates and the solder were assumed to be linearly elastic and isotropic, and thermal
residual stresses generated during the soldering process were assumed to be negligible. Plane strain
conditions were assumed for the solder layer since the solder deformation was highly constrained
by the adjacent substrates which were also assumed to be under plane strain. However, negligible
differences were observed when a state of plane stress was assumed for the substrates [31].

Equilibrium conditions of the infinitesimal representative element (Fig. 3.4(b)) give:

\[
\frac{dN_1}{dx} = -\tau, \quad \frac{dN_2}{dx} = \tau
\]

(1)
\[
\frac{dV_1}{dx} = -\sigma, \quad \frac{dV_2}{dx} = \sigma
\]  
\[\text{(2)}\]

\[
\frac{dM_1}{dx} = V_1 - \tau \frac{h + t}{2}, \quad \frac{dM_2}{dx} = V_2 - \tau \frac{h + t}{2}
\]  
\[\text{(3)}\]

Assuming that the thicknesses of the substrates and the solder layer are small compared to their lengths and widths, first order shear deformation theory, also known as Timoshenko beam theory, was used to describe deformations of the substrates. The internal forces in the substrates are then

\[N = A \frac{du_0}{dx}, \quad V = C \left( \frac{dv_0}{dx} + \phi \right), \quad M = D \frac{d\phi}{dx}\]  
\[\text{(4)}\]

where \(u_0\) and \(v_0\) are the displacements of a point on the mid-plane of a substrate in the \(x\) and \(y\) directions (Fig. 3.4a), respectively, and \(\phi\) is the beam rotation in \(z\)-direction. Also \(A\), \(C\) and \(D\) are the extensional, shear, and bending stiffnesses of the Timoshenko beam, respectively.

The shear and peel stresses are assumed constant through the thickness of the thin solder layer and are thus related to substrate deformation as follows:

\[\sigma = E_s^* E_s = E_s^* \frac{V_{0.2} - V_{0.1}}{t}, \quad E_s^* = \frac{E_s}{1 - \nu_s^2}\]  
\[\text{(5)}\]

\[\tau = G_s \gamma_s = \frac{G_s}{t} \left( u_{0.2} - u_{0.1} - \frac{h}{2} (\phi_2 + \phi_1) \right)\]  
\[\text{(6)}\]

with \(E_s\), \(G_s\) and \(\nu_s\) being the Young’s modulus, shear modulus and Poisson’s ratio of the solder.

Combining Eqs. (1-6), a 4th order linear ordinary differential equation for solder peel stress as well as a 3rd order differential equation for solder shear stress are obtained. Thus seven boundary conditions are required to find the integration constants. In Appendix 3A, the resulting expressions
for solder peel and shear stresses are substituted into Eqs. (1-3) and integrated with respect to \( x \) to obtain general expressions for the resultant forces acting on the substrate-solder-substrate sandwich of Fig. 3.4.

### 3.3.2. Double joint CFM

The double joint model of Fig. 3.5 consisted of two stiff substrates bonded by two discrete joints of length \( L \) with a gap between them of \( d \). The solution procedure described in Section 3.3.1 was modified to account for the gap between the joints.

The joint was divided into three regions as shown in Fig. 3.5, each with a local coordinate system having an origin at the left edge. In each region, \( N_{ij}, V_{ij}, \) and \( M_{ij} \) denote the axial force, shear force, and bending moment per unit width, respectively. The first subscript, \( i \), refers to the upper or lower substrate, while the second subscript, \( j \), refers to the region number. Superscripts \( L \) and \( R \) show the loads applied to left and right edge of each region, respectively. For example, \( V_{13}^L \) denotes the shear force acting on left edge of upper substrate at Region 3, as shown in Fig. 3.5.

The resultant forces in Region 1, 2 and 3 \((N_1, N_2, V_1, V_2, M_1, M_2)\) were obtained using Eqs. (3A.8) to (3A.13), with the constants \( m_i \) \((1 \leq i \leq 13)\) being different in each region. In addition to these integration constants, 24 unknown resultant forces at the vertical interface between the adjacent regions in the upper and lower substrate must be found.
Fig. 3.5. A substrate-solder-substrate sandwich element with two solder joints under generalized loading. The joint was divided into three regions.

The following 12 equations express the continuity conditions at the vertical interfaces between the adjacent regions in the upper and lower substrate:

\[
\begin{align*}
N_{11}^R &= N_{11}^L, & N_{21}^R &= N_{21}^L, & N_{12}^R &= N_{12}^L, & N_{22}^R &= N_{22}^L \\
V_{11}^R &= V_{12}^L, & V_{21}^R &= V_{22}^L, & V_{12}^R &= V_{13}^L, & V_{22}^R &= V_{23}^L \\
M_{11}^R &= M_{12}^L, & M_{21}^R &= M_{22}^L, & M_{12}^R &= M_{13}^L, & M_{22}^R &= M_{23}^L
\end{align*}
\]

(7)

The equilibrium equations for the upper substrate of Region 1 are

\[
\begin{align*}
N_{11}^R &= N_{11}^L - \int_0^L \tau_1 \, dx, & V_{11}^R &= V_{11}^L - \int_0^L \sigma_1 \, dx \\
M_{11}^R &= M_{11}^L + LV_{11}^R - \frac{L + h}{2} \int_0^L \tau_1 \, dx + \int_0^L x \sigma_1 \, dx
\end{align*}
\]

(8)
while those for the upper substrate of Region 2 are

\[ N_{12}^R = N_{12}^L, \quad V_{12}^R = V_{12}^L, \quad M_{12}^R = M_{12}^L + dV_{12}^R \]  \hspace{1cm} (9)

The equilibrium equations for Region 1 and 2 can be written as illustrated in Fig. 3.6. From Fig. 3.6(a),

\[ V_{11}^R + V_{21}^R = V_{11}^L + V_{21}^L, \quad N_{11}^R + N_{21}^R = N_{11}^L + N_{21}^L \] \hspace{1cm} (10)

\[ M_{11}^R + M_{21}^R = M_{11}^L + M_{21}^L + L(V_{11}^R + V_{21}^R) + \frac{t + h}{2} \left[ (N_{11}^R - N_{21}^R) - (N_{11}^L - N_{21}^L) \right] \]

From Fig. 3.6(b), the equilibrium equations can be written as:

\[ V_{12}^R + V_{22}^R = V_{12}^L + V_{22}^L, \quad N_{12}^R + N_{22}^R = N_{12}^L + N_{22}^L \] \hspace{1cm} (11)

\[ M_{12}^R + M_{22}^R = M_{12}^L + M_{22}^L + (L + d)(V_{12}^R + V_{22}^R) + \frac{t + h}{2} \left[ (N_{12}^R - N_{22}^R) - (N_{12}^L - N_{22}^L) \right] \]

Continuity and equilibrium conditions, Eqs. (7-11), were used to find the integration constants as well as the resultant forces at the interface between the regions. Once all integration constants were known, determining the stresses, resultants forces, and displacements at any point was straightforward.
3.3.3. **Discrete foundation model**

The CFM of Section 3.3.2 provided analytical expressions for the solder peel and shear stresses in a double-joint DCB, but the integration constants could be obtained only numerically. Therefore, this model did not yield an explicit expression for the maximum peel stress as a function of substrate stiffness and joint geometry. A simpler discrete foundation model (DFM) was developed by replacing the collective effects of each solder joint and the lower substrate with one or two tension-compression springs (Fig. 3.7a). The spring modulus was determined using the beam-on-elastic-foundation model of Penado [34] in which the foundation modulus has terms related to the bonding layer and the opposing substrate. This three-spring simplification was motivated by the assumption that the essential characteristics of the solder joint behavior are governed by the transverse tensile (peel) and compressive stresses generated by substrate bending.

As will be seen in Section 3.5.1, under bending loadings, for relatively wide spacing, \( d \), half of each joint is under tension while the other half is under compression, so each joint should be modelled using two springs, one in tension the other in compression. However, for more closely-spaced joints the second joint is loaded only in compression and the first joint is loaded mostly in tension with a small compressive zone. Therefore, in cases of practical interest the first joint can
be represented by two springs while the second joint can be modelled as a single compression spring. This greatly simplifies the model and allows for a closed-form solution.

![Image of three-spring model and free-body diagram]

**Fig. 3.7.** a) Schematic of the three-spring model, b) free-body diagram of the three-spring model

The three-spring model of Fig. 3.7 was used to model the double joint of Fig. 3.2a. The first solder joint of length \( L \) (Fig. 3.2a) was modeled using two springs \( d_1 \) apart. The third spring, representing the second solder joint, was located a distance \( d_2 \) (joint spacing of Fig. 3.2a) from the end of the first joint. The stiffness of each spring was defined so that the total stiffness of the first joint \( (K_1+K_2) \) equaled that of the second joint \( (K_3) \).

As will be seen below, this arrangement could capture the geometric and stiffness conditions leading to the full range of peel stress distributions in the first (left) joint of Fig. 3.2a. Moreover, this simplified model can be used to approximate the effects of the bending and transverse loads that generate solder peel stresses in any two-dimensional loading.

The equilibrium equations for the spring model of Fig. 3.7 are:

\[
F = F_1 + F_2 + F_3, \quad (a+d_1+d_2)F = (d_1+d_2)F_1 + d_2F_2
\]

This is a statically indeterminate structure, and an additional equation is required to find all
of the reactions. From Castigliano's theorem [36, 37]:

$$\frac{\partial U}{\partial F_1} = 0$$

(13)

where $U$ is the total strain energy of the system given by

$$U = \int_0^a \frac{M^2}{2EI} dx + \int_a^{a+d_1} \frac{M^2}{2EI} dx + \int_{a+d_1}^{a+d_1+d_2} \frac{M^2}{2EI} dx + \frac{F_1^2}{2K_1} + \frac{F_2^2}{2K_2} + \frac{F_3^2}{2K_3}$$

(14)

where the contribution of shear is neglected and $M$ is the internal bending moment of the beam, $I$ is the moment of inertia of the beam cross-section, $E$ is the beam Young’s modulus, and $K$ is the spring stiffness given by [34-37]:

$$K_1 = K_2 = K_3 = K = \frac{c_c L}{h} \left( \frac{1 - \nu_s^2}{\frac{t}{E b} + \frac{b E_s}{b E_s}} \right)$$

(15)

where $E_s$ and $\nu_s$ are the solder Young’s modulus and Poisson’s ratio, respectively, and $t$ is the solder layer thickness. The lower substrate thickness and width are $h$ and $b$, respectively, while the modulus of the lower substrate is taken here to be the same as that of the upper beam. The solder joint length is $L$, equal to $d_1$ for the three-spring model (Fig. 3.7). Thus the foundation modulus that is modelled by the discrete springs contains contributions from both the solder layer and the opposing substrate.

The calibration constant, $c_c$, in Eq. (15) was determined using a finite element model (FEM) of an arbitrary configuration of the type of joint being analyzed. The loading and boundary conditions in the FEM matched those of Fig. 3.5, along with an arbitrary selection of the parameters of Eq. (15). The peel stresses in the FEM were then summed along the joint regions attributed to each spring (Fig. 3.7) and divided by the displacements in the opening direction ($v$,
Fig. 3.4). The calibration constant, $c_c$, was then adjusted to give a good agreement between the predictions of Eq. (15) and the stiffness as predicted by the calibration FEM. Using a similar approach, Kanninen [35] found a value of $c_c=2$ for a homogeneous DCB joint, while Penado [34] used $c_c=4$ for an adhesively-bonded DCB joint. In this study, $c_c=4$ and $c_c=0.5$ were used, respectively, for the DCB joint of Fig. 3.2a, and the chip resistors of Figs. 3.18 and 3.22, described in Section 3.5.3.

Substituting Eq. (14) into Eq. (13) and simplifying yields the following close-form solution for $F_1$, the force in the first spring:

$$
\frac{F_1}{F} = \frac{2d_1^2d_1^3 + d_1^2 \left( 2d_1^2 + 3ad_2^2 + 9c \right) + d_1 \left( 2a d_1^2 + 12cd_2 + 9ac \right) + 6c d_2^2 + 6acd_2}{2d_2^2d_1^3 + d_1^2 \left( 2d_2^2 + 9c \right) + 12cd_1d_2 + 12cd_1^2}
$$  (16)

where $c=EI/K$. $EI$ indicates the bending stiffness of the upper substrate (the beam shown in Fig. 3.7). As will be shown in Section 3.5, $F_1$ in the three-spring model of Fig. 3.7 correlates very well with the maximum peel stress in the double joint of Fig. 3.2a. Therefore, Eq. (16) provides a useful means of understanding the effects of changes in different parameters on load sharing in arbitrary double-joint configurations.

According to Eq. 16, the important parameters affecting $F_1$ can be divided into two categories: geometric parameters including joint length ($d_1$), joint spacing ($d_2$), substrate thickness ($h$), and joint thickness ($t$) as well as material parameters including substrate Young’s modulus ($E$) and joint Young’s modulus ($E_s$). In a given surface mount microelectronics assembly, the solder joint thickness ($t$) and the Young’s modulus of the substrate ($E$) and the solder ($E_s$) are often fixed, and the only parameters that can be changed to optimize the load sharing among the joints are joint length ($d_1$), joint spacing ($d_2$), and substrate thickness ($h$). The effect of these three independent parameters on load sharing in different bending configurations will be investigated further in the
following sections.

3.4. Finite element analysis

A finite element model of the configurations of Fig. 3.2 was developed using ANSYS to verify the analytical results of the solder stress distribution. Copper substrates ($E=124$ GPa, $\nu=0.35$ GPa) and solder ($E_s=41$ GPa, $\nu_s=0.4$, GPa) were modeled as linear elastic materials under plane stress and plane strain conditions, respectively. To minimize the required number of elements, biased meshing was used for the substrates and solder, with an increased mesh density in regions that experience significant stress gradients (Fig. 3.8), including the vicinity of the solder layer as well as the copper substrates near the joints. A convergence study was performed to find the appropriate mesh density. To represent the boundary conditions, both translational degrees of freedom ($u_x, u_y$) of the bottom-most point of the pin hole of the lower substrate were constrained, while a point force was applied to the top-most point of the pin hole of the upper substrate. However, it was found that the exact location of these points had a negligible effect on the stress results. The fracture loads obtained from the experimental measurements were used in the finite element model to calculate stresses in the mid-plane of the solder layer.
Fig. 3.8. FE model of the double joint DCB (Fig. 3.2a) with an enlarged view of the joint area.

3.5. Results and discussion

Section 3.5.1 presents the key trends of the solder peel stress distribution in the single and double joint model bending specimens using primarily the predictions of the CFM and FEM. Comparisons with the experimental data are in Section 3.5.2, while Section 3.5.3 considers a specific example of the stresses in a pair of solder joints connecting a resistor to a circuit board, with the assembly subject to two types of loading.

3.5.1. Solder peel stress distribution

Figure 3.9 shows the variation of the maximum peel stress, $\sigma_{y,\text{max}}$, with the joint length for the single-joint mode I DCB configuration (Fig. 3.2b) as estimated using the FEM and the CFM. The applied load was arbitrarily set as $F=50$ N, which was almost equal to the measured failure force for the single 2 mm joint. Assuming the maximum peel stress as the fracture criterion for solder joints, a higher maximum peel stress under a constant applied load indicates a lower fracture strength.

Figure 3.9 shows that there is a good agreement between the FEM and the CFM. Both predicted that after a certain characteristic length, $L_{cr}$, the maximum peel stress, and therefore the joint strength, remain unchanged. For the specific configuration of Fig. 3.2(b), it can be estimated that $L_{cr} = 15$ mm. The large values of the peel stresses near the left edge of the leading solder joint imply that fracture will occur there.
Fig. 3.9. Maximum solder peel stress estimated by FEM and CFM at the mid-plane of solder layer of a single-joint mode I DCB (dimensions and properties of Fig. 3.2b) for different joint lengths and $F=50$ N.

Figure 3.9 implies that if the first solder joint in a double joint configuration (Fig. 3.2a) is longer than the characteristic length ($L_{cr}=15$ mm), the maximum peel stress is independent of the presence of any solder joint to the right and there will be no load sharing between the two joints. In such cases the failure of the double joint is due to the maximum peel stress in the first joint, and the second joint has no effect on the failure load. Therefore, load sharing occurs only if the length of the leading (first) joint is less than $L_{cr}$. In this study, 2 mm joints were used to investigate the trends seen in the maximum peel stress as a function of joint geometry and the stiffnesses of the solder and substrates.

Figure 3.10 compares the variation of the peel stress along the mid-plane of a 2 mm long single joint as well as the leading joint of a double-joint configuration for joint gaps of $d=1$ mm and $d=20$ mm. In the double-joint configuration, the length of each joint was 2 mm. Figure 3.10 shows the addition of a second joint with a joint gap of $d=1$ mm significantly reduced the peel...
stress in the leading joint, thereby strengthening the joint. The addition of a second joint with a joint gap of \( d = 20 \) mm reduced the peel stress at the leading edge even further. The effect of joint gap on the peel stress will be discussed in detail in the following sections.

The CFM and the FEM display the same trends, but the CFM underestimated the tensile peel stress near the left edge and overestimated the compressive peel stress near the right edge. Figure 3.10b shows this more clearly near the left edge of the leading solder layer. The main reason for these differences was the assumption in the CFM that the transverse shear strain varied linearly through the thickness of the DCB substrate, which was 12.7 mm (Fig. 3.2). Since the beam length to thickness ratio was just 0.16 over the 2 mm length of the joint, the transverse shear strains varied nonlinearly through the thickness of the beam, and greater accuracy would require a higher-order beam theory [38, 39]. Since for short joints, the FEM was more accurate than the CFM, the following discussions make use of the predictions of the FEM.
Fig. 3.10. FEM and CFM predictions of solder peel stress along the mid-plane of a 2 mm long joint for different single and double-joint mode I DCB configurations (dimensions and properties of Fig. 3.2). The applied load was $F=50$ N. (a) Peel stress distribution along the length of the leading single and double solder joint and (b) peel stresses in vicinity of the left edge of the leading solder joint.
Figure 3.11 shows that the peel stresses in the second 2 mm joint of the double-joint configuration are compressive for $1 \leq d \leq 20$ mm, since they act together with the tensile peel stresses in the leading joint (Fig. 3.10) to provide overall moment equilibrium in the DCB. When the gap between the joints is very large, such as $d=100$ mm, the two joints begin to act independently so that the left half of the second joint is under compression while the right half is under tension, just as with the single joint case of Fig. 3.10a.

![Graph showing FEM predictions of solder peel stress](image)

**Fig. 3.11.** FEM predictions of solder peel stress along the mid-plane of the second solder joint (right hand joint) of the double-joint mode I DCB (dimensions and properties of Fig. 3.2) for various joint spacings, $d$. The applied load was $F=50$ N. Both joints are 2 mm long.

Figure 3.12 compares the distribution of the solder peel stress in a single 24 mm long joint and a double-joint configuration of 2 mm joints with a 20 mm gap between them. As in Fig. 3.10a, the peel stress in the single joint was tensile near the left edge and compressive from about $x=5$ mm to the end of the joint. In the double joint, however, the two joints share the load, with the left joint under tension and the right joint under compression. The maximum predicted peel stress was
35 MPa for the single joint and 36 MPa for the double joint; therefore, the presence of the gap did not affect the maximum peel stress in the overall joint, and the fracture load of the two configurations should be the same, as discussed in the next section.

Fig. 3.12. FEM predictions of the effect of a gap on peel stress distribution for a mode I DCB (dimensions and properties of Fig. 3.2b) with a single solder joint having a 24 mm overall length. The applied load was $F=50$ N.

Figure 3.13 shows that the maximum peel stress in the first joint of the double-joint DCB of Fig. 3.2a was minimized at the optimal spacing $d_{cr}=20$ mm, where the load sharing, and therefore the joint strength, were maximum. For smaller gaps, $d$, the overall strength of the joint pair is reduced in the same way as it is for single joints of the same overall length (Fig. 3.9). For larger gaps each joint begins to act independently, with each having tensile and compressive zones, as shown in Figs. 3.10 and 3.11. At the optimal spacing, $d_{cr}$, the two joints act together in the most effective way with the first joint being essentially in tension while the second joint is in compression, as seen in Fig. 3.12.
Figure 3.14 shows that the optimal spacing $d_{cr}=20$ mm was also predicted accurately using the DFM developed in Section 3.3.3. The predictions of the force in the leading spring ($F_1$ in Fig. 3.7) were compared with FEM by integrating the peel stress along the left half of the leading joint.

**Fig. 3.13.** FEM predictions of maximum peel stress versus joint spacing in the double-joint mode I DCB of Fig. 3.2a and Figs. 3.10-3.11. The applied load was $F=50$ N.
Fig. 3.14. FEM and DFM predictions for solder peel resultant force as a function of joint spacing for the left half of the leading joint of a double-joint mode I DCB configuration as in Fig. 3.7 (dimensions and properties of Fig. 3.2a).

3.5.2. Measured fracture loads

Figure 3.15 shows the fracture loads that were measured in [31] for single-joint mode I DCB specimens of different joint lengths with the configuration and properties of Fig. 3.2b. For relatively short joints, the fracture load increased with the joint length, but reached a plateau starting at a characteristic length $L_{cr} = 15$ mm. This was consistent with the solder peel stress predictions of Fig. 3.9 ($L_{cr} = 15$), and confirmed the assumption that the peel stress is the dominant factor in crack initiation and fracture.
Fig. 3.15. Measured fracture loads of single solder joints with different lengths in the mode I DCB with the dimensions and properties of Fig. 3.2b, as reported in [31]. Error bars represent ±1 standard deviation based on 5 repeat experiments at each length.

Figure 3.16 shows the effect of joint spacing on the strength of a double joint with the configuration and properties of Fig. 3.2a, for a solder joint length $L=2$ mm. The measured fracture load increased with $d$ for small spacing, and reached a maximum near $d=d_{cr}\approx 20$ mm where the peel stress was minimum and optimal load sharing occurred (Figs. 13-14). This is consistent with the solder peel stress predictions of Fig. 3.13 for the same DCB configuration, as well as with the DFM predictions of Fig. 3.14; however, the exact location of the maximum could not be determined accurately since the differences in the fracture loads were insignificant (t-test, 95% confidence level) in the range $d=10$-40 mm. A comparison of Figs. 3.15 and 3.16 indicates that the fracture load of a single joint (about 1,150 N for $L>15$ mm) was very close to that of a double joint (about 1,050 N, $L=2$ mm, 10 mm$<d<40$ mm). This is consistent with Fig. 3.12, where there was little difference between the maximum peel stresses predicted in single and double-joint
configurations.

**Fig. 3.16.** Measured fracture loads of double solder joints for various joint spacing in a mode I DCB configuration and dimensions and properties of Fig. 3.2a. Error bars represent ±1 standard deviation based on 5 repeat experiments at each length.

Figure 3.17. shows typical fracture surfaces of single and double solder joints. Consistent with the observations of [31, 32, 40], the fracture surface of solder joints at the intermediate strain rates used in the present experiments was smooth and macroscopically flat. During solder reflow, the solder reacts with the copper substrates to create intermetallic compounds (IMC), hence forming a strong metallurgical bond. Inspections of the microstructure of the fracture surfaces using scanning electron microscopy (SEM) and the energy-dispersive X-ray spectroscopy (EDX) showed a mixture of both brittle fracture of Cu$_6$Sn$_5$ grains in the IMC and ductile fracture through the solder layer. In all specimens, the crack grew close to the more highly-strained substrate (the upper substrate in Figs. 3.2-3.6) at the interface between the IMC layer and the bulk solder.
Fig. 3.17. A typical fracture surface of a) a single continuous joint, b) a double joint with a joint spacing of 40 mm.

These results are for the specific case of a mode I DCB specimen (Fig. 3.2), and will change for a different loading. However, the trends of the variation of the peel stress with joint length or joint spacing are also observed in other types of loading, as illustrated in the following section for a pair of solder joints in a microelectronics chip resistor subject to two types of bending loads. The first loading is a variation of the double-joint DCB configuration of Fig. 3.2a and was included to validate the DFM model using solder joints of a scale seen in microelectronics. The second loading typifies that generated by circuit board bending.

3.5.3. Case study: chip resistor

Following the above approach, the optimal (characteristic) spacing of solder joints in surface-mount microelectronics assemblies subject to bending loads can be found from the variation of the peel stress as a function of the joint spacing. In the first example, Fig. 3.18 shows a chip resistor subject to asymmetric PCB bending. For simplicity, the free surface of the chip was assumed to be fixed. Using a loading arm length of $L_F=3$ mm and the dimensions and properties of Table 3.1, a finite element model was used to calculate the change of the maximum solder peel
stress with joint spacing as shown in Fig. 3.19.

![Diagram of chip resistor under PCB bending]

**Fig. 3.18.** A chip resistor under PCB bending, the free surface of the chip is fixed. Properties of Table 3.1.

**Table 3.1** Dimensions and material properties of a typical chip resistor [8]

Symbols defined in Fig. 3.18.

<table>
<thead>
<tr>
<th>Layers</th>
<th>$h$ (mm)</th>
<th>$L$ (mm)</th>
<th>$E$ (GPa)</th>
<th>$\nu$</th>
</tr>
</thead>
<tbody>
<tr>
<td>PCB (1)</td>
<td>1.25</td>
<td>9.6</td>
<td>22</td>
<td>0.28</td>
</tr>
<tr>
<td>Solder (2)</td>
<td>0.12</td>
<td>0.5</td>
<td>41</td>
<td>0.4</td>
</tr>
<tr>
<td>Resistor (3)</td>
<td>0.65</td>
<td>6.5</td>
<td>131</td>
<td>0.3</td>
</tr>
</tbody>
</table>

The trend is similar to that of Fig. 3.13 for the double-joint mode I DCB specimen, although the optimal joint spacing where the load sharing and joint strength were maximized was approximately 1 mm in Fig. 3.19 and about 20 mm in Fig. 3.13. Thus the optimal joint spacing is seen to be highly dependent on the stiffness of the substrates, being larger for stiffer substrates.
Fig. 3.19. FEM predictions of the maximum peel stress versus joint spacing for the chip resistor of Fig. 3.18, the properties of Table 3.1, and the applied load $F=1$ N.

Figure 3.20a shows the FEM predictions of the maximum peel stress in the leading (left hand) joint of Fig. 3.18 as a function of the joint spacing for three different solder joint lengths. It is seen that the optimal joint spacing (location of minimum stress) decreased with increasing joint length, with values of 1.6, 1.2 and 1.0 mm for the solder lengths of 0.3, 0.4 and 0.5 mm, respectively. However, in this particular example, the three minimum values of $\sigma_{y\text{max}}$ were approximately the same, being between 13 and 14 kPa for the three solder joint lengths, implying that the resistor strength would be independent of solder joint length provided that the optimal joint spacing was used in each case. The three solder joint lengths studied ($L_2=0.3$, 0.4, and 0.5 mm in Fig. 3.20) were smaller than the characteristic length which was predicted to be about 1 mm, and so in each of these situations the peel stresses in the leading joint were influenced by the solder gap and second joint.

For this same configuration, Figs. 3.20b and 3.20c compare the FEM and three-spring DFM
(Fig. 3.7, Eq. (16)) predictions of the variation of the resultant peel force in the left half of the first joint \( F_1 \) as a function of the joint spacing. It is seen that the trends predicted by the DFM agreed quite well with those of the FEM; i.e. the predicted optimal joint spacings for the 0.3, 0.4 and 0.5 mm solder joints were, respectively, 1.5, 1.1 and 0.8 mm for the DFM, and 1.2, 1.0, and 0.7 mm for the FEM. In both cases, the predicted optimal spacing decreased with increasing solder joint length, just as with the FEM predictions of \( \sigma_{y \text{max}} \) in Fig. 3.20a.

Although the DFM predictions of the optimal spacing were consistent with the FEM, the DFM incorrectly predicted that shorter solder joints have a larger maximum strength at their particular optimal spacing \( d_{cr} \) corresponding to the minimum \( F_1/F \) in Fig. 3.20c. Fig. 3.20a showed that the opposite was true, although as mentioned above, the effect on \( \sigma_{y \text{max}} \) was relatively small. The reason for this discrepancy was the inability of a single spring force, \( F_1 \), to reflect the actual \( \sigma_{y \text{max}} \) distribution in the vicinity of the end of the first solder joint. Nevertheless, the present three-spring model was able to capture the effect of joint spacing quite accurately in spite of its simplicity. The following example will show that it also reflects the correct dependence on changes in substrate stiffness.

Figure 3.21 shows the FEM and DFM predictions of the effect of PCB thickness on the variation of \( \sigma_{y \text{max}} \) and \( F_1/F \) with joint spacing for the configuration of Fig. 3.18. It is seen that the optimal spacing (location of minima) increased with increasing PCB thickness, or equivalently the PCB stiffness, and that the predictions of the DFM were reasonably close to those of the FEM; i.e. the predicted optimal joint spacings for the 1.25, 1.5 and 1.75 mm PCBs were, respectively, 0.8, 1.3, and 1.7 mm for the DFM, and 0.7, 1.2, and 1.6 mm for the FEM. Moreover, the trend of increasing joint strength (decreasing \( \sigma_{y \text{max}} \)) with increasing PCB thickness seen in Fig. 3.21a, was also correctly reflected in the variation of \( F_1 \) shown in Figs. 3.21b and 3.21c.
Fig. 3.20 a) FEM predictions of maximum peel stress versus joint spacing for the first joint in the chip resistor with the configuration of Fig. 3.18, and the applied load $F=1$ N. Three different joint lengths, $L_2$, were considered. b-c) FEM and DFM (three-spring model) predictions of the effect of joint spacing on the resultant peel force, $F_1$, acting on the left half of the first (left hand) joint in the chip resistor with the configuration of Fig. 3.18.
Fig. 3.21 a) FEM predictions of maximum peel stress versus joint spacing for the first joint in the chip resistor with the configuration of Fig. 3.18, and the applied load $F = 1$ N. Three different PCB thicknesses, $h_1$, were considered. b-c) FEM and DFM (three-spring model) predictions of the effect of joint spacing on the resultant peel force, $F_1$, acting on the left half of the first (left hand) joint in the chip resistor with the configuration of Fig. 3.18.
As noted above, the variation of the maximum peel stress with joint spacing depends on the loading conditions. For example, Fig. 3.22 shows a chip resistor assembly under three-point bending with a PCB length $L_1=12.8$ mm, a chip resistor length $L_3=8.0$ mm, a loading arm, $L_F=1$ mm, and other dimensions and properties as in Table 3.1.

**Fig. 3.22.** A chip resistor under PCB three-point bending.

Figure 3.23 shows that the trend of the maximum peel stress with joint spacing is quite different from that of Fig. 3.19 for the DCB-type loading, indicating that load sharing decreases (maximum peel stress increases) as the spacing increases, reaching a worst-case spacing of about 3-4 mm before load sharing increases once again. Therefore, joints such that in Fig. 3.22 are strengthened by either having the joints as close as possible or by having them widely spaced.
Fig. 3.23 FEM predictions of the maximum peel stress versus joint spacing for the chip resistor configuration of Fig. 3.22, the properties of Table 3.1 (except that $L_1=12.8$ mm and $L_3=8.00$ mm), and the applied load $F=1$ N.

A three-spring discrete foundation model (Fig. 3.24a) was developed to capture the trend observed in Fig. 3.23. As in Section 3.3.3, the first joint was modeled using two springs, while the second joint was modeled using a single spring. The equilibrium equations and Castigliano's theorem yield the following close-form solution for $F_1$, the force in the first spring:

$$
\frac{F_1}{F} = -\frac{(L_2^2 (24L_p + 16L_2)/96 - (L_1L_2^2)/8 - (L_1L_2d_2)/8 + (L_2d_2 (24L_p + 24L_2 + 8d_2))/96 + (L_2^2 (3L_2^2 + 20L_2d_2 + 4L_pL_2 + 12d_2^2 + 24L_p d_2))/(96L_1) - (L_2d_2 (9L_2^2 + 12L_pL_2 - 4d_2^2 - 8L_p d_2))/(96L_1))/(c + (L_2^2 d_2)/3 + L_2^3/3 + (c(L_2 + d_2^2))}/d_2^2 + (cL_2^2)/(2d_2^2))
$$

(17)

where $d_2=d+L_2/2$, and $c=EI/K$. $EI$ indicates the bending stiffness of the PCB. $K$ is the spring stiffness given by Eq. 15. The dimensions are defined in Fig. 3.22 and Fig. 3.24a and Table 3.1.
Figure 3.24b shows that the DFM predicted the variation of $F_i$ with the joint spacing quite accurately, yielding a worst possible spacing (maximum $F_i$) of approximately 3.5 mm, in close agreement with the FEM predictions of $\sigma_{\text{max}}$ in Fig. 3.23.

Fig. 3.24 (a) Three-spring DFM model used to represent the chip resistor with the configuration of Fig. 3.22, the properties of Table 3.1 (except that $L_1=12.8 \text{ mm}$ and $L_3=8.0 \text{ mm}$), (b) DFM predictions of the force of the first spring versus joint spacing, $d = d_2-L_2/2$.

Figures 3.25 and 3.26 portray the effect of the solder joint length and the PCB thickness on the variation of $\sigma_{\text{ymax}}$ and $F_i / F$ with joint spacing, where $F_i$ and $F$ are defined in Fig. 3.24a. It is seen that the worst possible joint spacing (maximum $F_i$ or $\sigma_{\text{ymax}}$) increases as the solder joints
become shorter (Fig. 3.25d) and as the circuit boards becomes thicker (Fig. 3.26d). Comparison of Figs. 3.20-3.21 and Figs. 3.25-3.26 shows that the critical joint spacing (the optimal spacing for the PCB-resistor assembly with the configuration of Fig. 3.18, and the worst spacing for the configuration of Fig. 23.2) always decreases with the solder joint length, and increases with the PCB thickness.

Figure 3.25d shows that the DFM and FEM predictions of the worst joint spacing were very close for the solder lengths of 0.4, 0.5 and 0.6 mm in the assembly of Fig. 3.22. Similarly, Fig. 3.26d shows that the DFM and FEM predictions of the worst joint spacing for the 1.25, 1.5 and 1.75 mm thick PCBs in Fig. 3.22 were also quite close to each other.
**Fig. 3.25** a) FEM predictions of maximum peel stress versus joint spacing for the first joint in the chip resistor with the bending configuration of Fig. 3.22, and the applied load $F=1$ N. Three different joint lengths, $L_2$, were considered. b-c) FEM and DFM (three-spring model) predictions of the effect of joint spacing on the resultant peel force, $F_1$, acting on the left half of the first (left hand) joint in the chip resistor with the configuration of Fig. 3.22. d) Worst joint spacing for different joint lengths based on the maximum $F_1$ or the maximum peel stress.
Fig. 3.26  a) FEM predictions of maximum peel stress versus joint spacing for the first joint in the chip resistor with the bending configuration of Fig. 3.22, and the applied load $F=1$ N. Three different PCB thicknesses, $h_1$, were considered. b-c) FEM and DFM (three-spring model) predictions of the effect of joint spacing on the resultant peel force, $F_i$, acting on the left half of the first (left hand) joint in the chip resistor with the configuration of Fig. 3.22. d) Worst joint spacing for different PCB thicknesses based on the maximum $F_i$ or the maximum peel stress.
3.6. Conclusions

The strength of a single solder joint in a multi-joint configuration subject to bending loads depends on the load sharing between the adjacent joints. Load sharing can be optimized to decrease the maximum solder peel stresses, and hence increase joint strength, by the suitable design of joint spacing for a given load and solder joint dimensions and properties.

Copper-solder-copper DCB specimens with two discrete 2 mm long solder joints were fabricated and tested under mode I loading for a range of joint spacings. It was found that the maximum strength was achieved at a certain optimal joint spacing that corresponded to the minimum peel stress as predicted using the FEM and the DFM.

Solder peel stresses were calculated using both an FE model and an analytical continuous foundation model (CFM), in which the solder joint was isolated from the surrounding structure as a substrate-solder-substrate sandwich. In the sandwich, the substrates were modeled as Timoshenko beams and the solder layer as normal and shear springs. The CFM accounted for the gap between two discrete solder joints, and was in reasonable agreement with the FEM, although it underestimated the maximum peel stress. A simpler, discrete foundation model (DFM) was developed to provide a closed-form expression for the resultant peel force acting on the critical solder joint in a double solder-joint configuration loaded in bending. Its predictions of the effect of joint spacing were in good agreement with those of the FEM for model DCB-type configurations and a microelectronics chip resistor on a printed circuit board under bending.
Appendix 3A. General expressions for the resultant forces

Combining Eqs. (1-6), a linear ordinary differential equation for solder peel stress is obtained:

\[
\frac{d^4 \sigma}{dx^4} + \eta_1 \frac{d^2 \sigma}{dx^2} + \eta_2 \sigma = 0, \quad \eta_1 = -\frac{2 E_s^*}{t C}, \quad \eta_2 = \frac{2 E_s^*}{t D}
\]  

(3A.1)

The roots of the characteristic equation are:

\[
r_{01,02} = \frac{1}{\sqrt{2}} \sqrt{-\eta_1 \pm \sqrt{\eta_1^2 - 4 \eta_2}}
\]  

(3A.2)

Depending on the values of \( h_1 \) and \( h_2 \), the roots of the characteristic equation could be real or complex. \( r_{01} \) and \( r_{02} \) are real if:

\[
\eta_1^2 - 4 \eta_2 > 0 \quad \Rightarrow \quad \frac{h}{t} > 6 k^2 \frac{E}{E_s^*} \frac{(1 - \nu^2)}{(1 + \nu)^2}
\]  

(3A.3)

\( k \) is equal to 5/6 for a Timoshenko beam with rectangular cross section [41]. Assuming \( \nu = 0.3 \), Eq. (3A.3.) is simplified as follows:

\[
\frac{h}{t} > 2.24 \frac{E}{E_s^*}
\]  

(3A.4)

Therefore, the Young’s modulus of the substrate and the solder must satisfy Eq. (3A.4) to ensure the characteristic equation has real roots. For example, in a solder joint (\( E_s = 41 \) GPa, \( t = 0.25 \) mm) with copper substrates (\( E_s = 124 \) GPa), the substrate thickness must be \( h > 1.4 \) mm to ensure the existence of real characteristic roots. Similarly, in a typical adhesive joint (\( E_s = 2.5 \) GPa, \( t = 0.25 \) mm) with aluminium substrates (\( E_s = 70 \) GPa), the substrate thickness must be \( h > 13.2 \) mm to guarantee the existence of real characteristic roots. The peel stress equation, Eq. (3A.1), thus has
the following general solution:

\[
\sigma(x) = m_1 \sinh(r_{01}x) + m_2 \cosh(r_{01}x) \\
+ m_3 \sinh(r_{02}x) + m_4 \cosh(r_{02}x)
\]  

(3A.5)

Similarly, a differential equation for shear stress is obtained by combining Eqs. (1-6):

\[
\frac{d^3\tau}{dx^3} + \xi_1 \frac{d\tau}{dx} = 0, \quad \xi_1 = -\frac{2G_s}{t} \left( \frac{1}{A} + \frac{h(h+t)}{4D} \right)
\]  

(3A.6)

The characteristic equation of the shear stress always has real roots. Thus, the general solution of shear stress is

\[
\tau(x) = m_5 + m_6 \sinh(r_{03}x) + m_7 \cosh(r_{03}x), \quad r_{03} = \sqrt{-\xi_1}
\]  

(3A.7)

Substituting Eqs. (3A-5) and (3A-7) into Eqs. (1-3) and integration with respect to \(x\) give:

\[
N_1 = -m_5x - \frac{m_6}{r_{03}} \cosh(r_{03}x) - \frac{m_7}{r_{03}} \sinh(r_{03}x) + m_8
\]  

(3A.8)

\[
N_2 = m_5x + \frac{m_6}{r_{03}} \cosh(r_{03}x) + \frac{m_7}{r_{03}} \sinh(r_{03}x) + m_9
\]  

(3A.9)

\[
V_1 = -\frac{m_1}{r_{01}} \cosh(r_{01}x) - \frac{m_2}{r_{01}} \sinh(r_{01}x) \\
- \frac{m_3}{r_{02}} \cosh(r_{02}x) - \frac{m_4}{r_{02}} \sinh(r_{02}x) + m_{10}
\]  

(3A.10)

\[
V_2 = \frac{m_1}{r_{01}} \cosh(r_{01}x) + \frac{m_2}{r_{01}} \sinh(r_{01}x) \\
+ \frac{m_3}{r_{02}} \cosh(r_{02}x) + \frac{m_4}{r_{02}} \sinh(r_{02}x) + m_{11}
\]  

(3A.11)
\[ M_1 = -\frac{m_1}{r_{01}^2} \sinh(r_{01}x) - \frac{m_2}{r_{01}^2} \cosh(r_{01}x) \]
\[ -\frac{m_3}{r_{02}^2} \sinh(r_{02}x) - \frac{m_4}{r_{02}^2} \cos(r_{02}x) + m_{10}x \] 
\[ = \frac{h + t}{2} \left( m_5x + \frac{m_6}{r_{03}} \cosh(r_{03}x) + \frac{m_7}{r_{03}} \sinh(r_{03}x) \right) + m_{12} \tag{3A.12} \]

\[ M_2 = \frac{m_1}{r_{01}^2} \sinh(r_{01}x) + \frac{m_2}{r_{01}^2} \cosh(r_{01}x) \]
\[ + \frac{m_3}{r_{02}^2} \sinh(r_{02}x) + \frac{m_4}{r_{02}^2} \cos(r_{02}x) + m_{11}x \] 
\[ = \frac{h + t}{2} \left( m_5x + \frac{m_6}{r_{03}} \cosh(r_{03}x) + \frac{m_7}{r_{03}} \sinh(r_{03}x) \right) + m_{13} \tag{3A.13} \]

Therefore 13 boundary conditions are needed to find \( m_1 \) to \( m_{13} \). According to Fig. 3.4, the first 12 boundary conditions are:

\[ N_1(0) = N_1^L, \quad N_1(L) = N_1^R, \quad N_2(0) = N_2^L, \quad N_2(L) = N_2^R \]
\[ V_1(0) = V_1^L, \quad V_1(L) = V_1^R, \quad V_2(0) = V_2^L, \quad V_2(L) = V_2^R \]
\[ M_1(0) = M_1^L, \quad M_1(L) = M_1^R, \quad M_2(0) = M_2^L, \quad M_2(L) = M_2^R \]  
\[ \tag{3A.14} \]

The last equation is obtained by substituting Eqs. (3A.8), (3A.9), (3A.12) and (3A.13) into Eq. (4), and integrating with respect to \( x \) to obtain expressions for \( u_1, u_2, \phi_1, \) and \( \phi_2 \). The resulting expressions are then substituted into Eqs. (6) to obtain expressions for the shear stresses, which will be equalized to Eq. (3A.7) to get the last equation:

\[ 2D \left( m_9 - m_8 \right) - Ah \left( m_{12} + m_{13} \right) = 0 \]  
\[ \tag{3A.15} \]
3.7. References


Chapter 4

4. Effect of adhesive fillet geometry on bond strength between microelectronic components and composite circuit boards

4.1. Introduction

Underfill adhesives are used commonly in microelectronics to enhance the reliability of the ball grid array (BGA) solder joints that connect complex components to composite printed circuit boards (PCBs). In board-level underfilling, the underfill, which is normally an unfilled or filled epoxy adhesive, is dispensed along the edges of the component and flows into the gap beneath the component and between the solder balls through the capillary action. It is then thermally cured to improve the reliability of the solder balls between BGA packages and PCBs [1-4]. To increase the underfill Young’s modulus and reduce its coefficient of thermal expansion (CTE), low-CTE silica fillers at high weight fractions are commonly added to underfill materials [5-9].

An important reliability issue in microelectronic assemblies is delamination at the interfaces between the layers of dissimilar materials in the component-solder-PCB sandwich as a result of PCB or substrate bending during board assembly, shipment, handling, and end use [10-12]. To study these interlaminar failures, test specimens must generate the relevant loading conditions and the resulting stress states.

Many studies have shown the strength of an adhesive joint may be significantly influenced by small variations in the local geometry at the end of the overlap region [13-17]. A number of studies [1-4] have used three-point and four-point bending experiments to study the reliability of
the adhesive joints bonding microelectronic components to PCBs. However, the effect of the size and shape of the underfill fillet on the bending strength of BGA-PCB assemblies has not been quantified and isolated from the effects of adhesive mechanical and thermal properties.

The objective of the present work was to understand the relationship between underfill fillet size on the crack initiation load and failure mode of underfilled BGA solder joints. The effect of underfill adhesive thermal and mechanical properties was examined using commercially-available filled and unfilled epoxies as well as a cyanoacrylate adhesive. The experimental results were verified with a detailed stress analysis conducted using a finite element model.

4.2. Experimental

4.2.1. Specimen preparation

The fracture performance of underfilled solder joints was investigated using assemblies of thin-profile fine-pitch ball grid array (TFBGA) packages (iNAND Embedded Flash Drives, SanDisk, Milpitas, USA; properties of Table 4.1) soldered to a 1 mm thick, multi-layer, solder-mask coated PCB (AT&S, Leoben, Austria). The PCB had a symmetric stackup as shown in Table 4.2.

The surface finish on the PCB copper pads was organic solderability preservative (OSP). The diameter and the height of the solder balls were 300 μm and 200 μm, respectively, after assembly. The solder paste applied on the board was Sn3.0Ag0.5Cu (SAC305) (Indium, New York, USA). The TFBGA packages contained a silicon die attached to a bismaleimide-triazine (BT) substrate, encapsulated in an epoxy molding compound (EMC).
Table 4.1. Properties of the BGA package [18-19]

<table>
<thead>
<tr>
<th>Material</th>
<th>Thickness (mm)</th>
<th>Young’s modulus (GPa)</th>
<th>Poisson’s ratio</th>
</tr>
</thead>
<tbody>
<tr>
<td>BT substrate</td>
<td>0.17</td>
<td>14.5</td>
<td>0.11</td>
</tr>
<tr>
<td>Silicon die</td>
<td>0.32</td>
<td>130</td>
<td>0.28</td>
</tr>
<tr>
<td>EMC</td>
<td>0.68</td>
<td>16.7</td>
<td>0.25</td>
</tr>
</tbody>
</table>

Table 4.2. PCB layers (symmetric about layer 9, total number of layers=17). SM=solder mask, PL=plated copper, RCC=resin coated copper, PR=prepreg.

<table>
<thead>
<tr>
<th>Layer No.</th>
<th>1</th>
<th>2</th>
<th>3</th>
<th>4</th>
<th>5</th>
<th>6</th>
<th>7</th>
<th>8</th>
<th>9</th>
</tr>
</thead>
<tbody>
<tr>
<td>Material</td>
<td>SM</td>
<td>PL</td>
<td>RCC</td>
<td>PL</td>
<td>PR</td>
<td>PL</td>
<td>PR</td>
<td>PL</td>
<td>PR</td>
</tr>
<tr>
<td>Thickness (μm)</td>
<td>20.0</td>
<td>28.0</td>
<td>50.0</td>
<td>28.0</td>
<td>50.0</td>
<td>28.0</td>
<td>190</td>
<td>17.5</td>
<td>200</td>
</tr>
</tbody>
</table>

Underfilling was performed immediately after solder reflow using one of two types of capillary underfills, each exhibiting a widely different set of mechanical and thermal properties: a silica-filled epoxy (Hysol 3537, Henkel Electronic Materials) or an unfilled epoxy (Hysol UF3808), with cure times of almost five minutes at temperatures of approximately 150°C. The underfilled BGA-PCB assemblies were fabricated using a fully-automated surface mount technology (SMT) assembly line (BlackBerry, London, ON, Canada). Some additional test specimens were made manually using a low-viscosity cyanoacrylate adhesive (Loctite 496) which produced a negligibly small fillet. The mechanical properties of these underfills are listed in Table 4.3, along with their abbreviated designations UF-A, UF-B, and CN.
Table 4.3. Properties of underfills as provided by the manufacturer.

<table>
<thead>
<tr>
<th>Properties</th>
<th>Hysol UF3808 (UF-A)</th>
<th>Hysol UF3537 (UF-B)</th>
<th>Loctite 496 (CN)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Type</td>
<td>Epoxy</td>
<td>Epoxy</td>
<td>Cyanoacrylate</td>
</tr>
<tr>
<td>Tensile Modulus (GPa)</td>
<td>2.6</td>
<td>4.3</td>
<td>1.7</td>
</tr>
<tr>
<td>Glass Transition Temperature (C)</td>
<td>113</td>
<td>118</td>
<td>165</td>
</tr>
<tr>
<td>Viscosity (mPa.s)</td>
<td>360</td>
<td>4,000</td>
<td>70</td>
</tr>
<tr>
<td>CTE (ppm/°C, T &lt;Tg)</td>
<td>55</td>
<td>47</td>
<td>80</td>
</tr>
<tr>
<td>CTE (ppm/°C, T &gt;Tg)</td>
<td>171</td>
<td>132</td>
<td>-</td>
</tr>
<tr>
<td>Filler weight fraction (wt%)</td>
<td>0</td>
<td>38</td>
<td>0</td>
</tr>
</tbody>
</table>

The test specimens were cut from the BGA-PCB assemblies using a precision circular saw with a diamond blade as indicated in Fig. 4.1. Fig. 4.2 shows the cross-section of a BGA-PCB assembly revealing the underfill layer, the solder joints in the vicinity of the underfill, copper trace layers and glass fibers through the thickness of the PCB.

The underfilled BGA-PCB assemblies were tested in a DCB configuration, as shown in Fig. 4.3. To fabricate the test specimens, the free surface of the BGA component was first sanded using a 400-grit sponge sander, then lightly wiped with acetone to remove contaminants. A loading arm consisting of a 1.5 mm thick piece of circuit board material (FR4, IS410, Isola, Chandler, Arizona, USA) was bonded to the BGA component surface using a room-temperature cure epoxy adhesive (Hysol E-40HT), taking care to avoid excess adhesive being squeezed from the joint and bonding to the PCB. The brass loading brackets were bonded to the PCB and the FR4 loading arms using a cyanoacrylate adhesive (Loctite 496). All specimens were 8 mm wide. Width independence of the fracture load results per unit width was established by testing specimens 4-16 mm wide.
**Fig. 4.1.** (a) Copper pads on PCB before package assembly (b) Packages mounted on PCB using reflow soldering and underfilling. The dashed lines show the saw cuts used to prepare the test specimens. Dimensions in mm.

**Fig. 4.2.** Cross-section of a BGA-PCB assembly underfilled with UF-A. The picture was taken parallel to the saw cuts shown in Fig. 4.1 (b).
Fig. 4.3. (a) Schematic of a DCB specimen showing the bonded FR4 loading arm and the loading brackets. Not to scale. (b) An untested DCB specimen.

As shown in Table 4.2 the PCB consisted of insulating layers of a woven glass-fiber epoxy composite, and conducting copper layers that have been etched during the lamination process to produce the required pattern of conducting traces. The places where the copper was etched away are filled with the resin of the next insulating layer during lamination. Therefore, the conducting layers can be considered as copper-epoxy composites [20]. Figure 4.4 shows the distribution of the copper traces in the conducting layers of the present PCBs. In addition to the glass-fiber epoxy composite layers (prepreg), layer 3 and layer 15 were resin-coated copper (RCC); i.e. electrodeposited copper traces coated with resin [21].

The tensile properties of the multi-layer PCBs were measured according to ASTM D3039 [22], giving a value $E_{PCB}=21.8$ GPa (5 specimens tested, standard deviation=5%). The PCB tensile behavior was almost linear until final fracture. This was due to the large volume fraction of the prepreg (77%) which was made of a relatively brittle epoxy resin and $E$-glass fibers [23-24]. The transverse tensile strength and Young’s modulus of the PCB, similar to other laminated fiber-reinforced polymeric composites, was much smaller than the longitudinal tensile strength and Young’s modulus [14, 16]. For example, ref. [16] reported that the transverse Young’s modulus
of laminated composites was typically just two or three times that of the neat epoxy matrix. However, for simplicity the PCB was modeled as a homogeneous, isotropic, elastic material ($E_{PCB}=21.8$ GPa) in the finite element analysis (FEA). As discussed in Section 4.3, this simplification had a negligible effect on the ability of the finite element model to predict the effect of the fillet size on the bending strength of underfilled BGAs.

Fig. 4.4. a) Top view of a BGA-PCB assembly after cutting and before bonding the FR4 arm, b) the distribution of copper traces in layers 2, 16, c) layers 4, 14, and d) layers 6, 8, 10, 12 of Table 4.2. The areas between the copper traces were filled with the laminate epoxy. The dashed line shows the transverse line of crack initiation from the region of the underfill fillet.

4.2.2. Modifications to the underfill fillet size

The size and shape of the original fillet formed during the underfilling process in the SMT line was dependent on the underfill viscosity, and the surface tensions of the underfill and the adjoining surfaces which controlled the contact angles. As shown in Figs. 4.5(a) and 4.6(a), the
underfill with a higher viscosity (UF-B) produced a larger fillet. In these cases the contact angles of the underfills were essentially the same since both were epoxies and the PCB and component were identical.

The fillet size was modified to investigate its effect on the fracture load and the failure pattern. Modifications to the original fillet fell into two categories: the reinforced (enlarged) fillet (Figs. 4.5(b) and 4.6(b)), and the damaged fillet (Figs. 4.5(c) and 4.6(c)).

**Fig. 4.5.** A BGA-PCB assembly with the unfilled underfill (UF-A in Table 4.3): a) as-manufactured fillet, b) reinforced fillet, c) damaged fillet. Dashed lines show the crack paths observed in the fracture tests. Fillet dimensions in mm.

**Fig. 4.6.** A BGA-PCB assembly with the silica-filled underfill (UF-B in Table 4.3): a) as-manufactured fillet, b) reinforced fillet, c) damaged fillet. Dashed lines show the crack paths observed in the fracture tests. Fillet dimensions in mm.
To enlarge the fillet, additional underfill was dispensed manually along the edge of the package from a syringe and cured as the original underfill, using a time-temperature profile that was verified with a thermocouple embedded in the added underfill of a calibration specimen. The reinforcement was done on specimens before they were cut from the PCB. The reinforced UF-A fillet seen in Fig. 4.5(b) was approximately the same size as the reinforced UF-B fillet shown in Fig. 4.6(b). Small differences in the color of the underfill fillet (Fig. 4.6(b)) were attributable to variations in the surface texture from sectioning and to differences in the lighting and digital color rendering during micrography.

The circular diamond saw was used to score the fillet, removing most of it, as shown in Figs. 4.5(c) and 4.6(c). In this condition, the original fillet was damaged to such an extent that it could no longer transfer significant load. Care was taken to ensure scoring the fillet created no damage in the PCB, as was verified by microscopic inspection after the cutting operation, and the consistency of the failure loads.

The fillet dimensions were measured for each fillet configuration and were used in a finite element model for stress analysis. Figures 4.5-4.6 show the average fillet dimensions of 5 specimens of each underfill configuration. The repeatability was good, with a standard deviation less than 10% in each dimension.

Some of the BGAs were underfilled manually with the cyanoacrylate adhesive (CN, Table 4.3), which had a much lower viscosity than either UF-A or UF-B. For this reason, there was no fillet in these specimens, and regarding fillet load transfer the BGAs underfilled with CN had effectively the same fillet as the damaged configurations of the BGAs underfilled with UF-A and UF-B (Figs. 4.5(c) and 4.6(c)).
4.2.3. Fracture tests

The DCB test specimens with the as-manufactured, damaged, and reinforced fillets were
loaded at a cross-head speed of 1.5 mm/min and the applied force was measured using a 200 N
load cell connected to a data acquisition board. The maximum measured load corresponded to
crack initiation, since the crack propagated unstably after initiation with no significant increase in
load due to crack toughening. The fracture surfaces were analyzed using scanning electron
microscopy (SEM) and energy-dispersive X-ray spectroscopy (EDX) to identify the crack paths
of the fractured specimens.

4.3. Finite Element Model

The BGA-PCB assembly described in Section 4.2.1 was modeled using 8-node rectangular
plane-strain elements (Plane183, ANSYS®15, Ansys Inc, Canonsburg, PA), as shown in Fig. 4.7,
with the elastic material properties of Tables 4.1 and 4.3. As will be discussed in Section 4.4.1,
cracking always initiated in the PCB. Cross-sectional optical micrographs of the BGA-PCB
assemblies (Figs. (4.5-4.6)) were used to provide the detailed shapes and average dimensions of
each underfill fillet. An interesting observation was that it was not necessary to model the solder
balls explicitly, and their connective role was replaced in the model by a continuous layer of the
underfill. Since the balls were quite distant from the underfill fillet, as shown in Fig. 4.2, and the
fillet was relatively stiff, the balls did not affect the stresses that caused crack initiation in the PCB
close to the underfill fillet. This was evident in a comparison of the stresses in models with and
without solder balls. Therefore, modelling the detailed geometry of the solder balls was an
unnecessary and time-consuming complication. To capture the steep stress gradients near the
underfill fillet, the element size was varied smoothly from the fillet to the areas far from the fillet
(Fig. 4.7).
As shown in Table 4.2 and Fig. 4.4, the PCB consisted of 17 insulating and conducting layers. The distribution of copper traces within each conducting layer was different, with variations occurring along the length of a given underfill fillet. This complex three-dimensional structure was simplified by treating the PCB as an isotropic homogeneous material so that the stresses could be viewed as locally average values. This is discussed further in Section 4.4.3.

The error introduced by modeling the PCB as an isotropic material was investigated by comparing the stresses near the fillet of Fig. 4.7 (model of the largest fillet) with those of a model having an orthotropic material with the same in-plane PCB Young’s modulus ($E_{PCB}=21.8$ GPa), but with a transverse modulus that was 90% smaller. This changed the maximum first principal stress in the PCB by only 10%. This insensitivity to the transverse Young’s modulus was expected since the PCB bending created in-plane normal stress that were much larger than the transverse components. Moreover, since the isotropic assumption was made in all of the FE models, any small errors would be comparable and systematic, so that the conclusions regarding the relative effects of fillet geometries would not be affected.
Thermal residual stresses were not considered due to a lack of cure shrinkage data and because their local values in the fillet region would also be complicated by the complex structure of the PCB. However, it was reasoned that the neglect of residual stress would introduce a constant error that would not affect the comparison of the effect of various fillet geometries.

4.4. Results

4.4.1. Fracture loads and failure modes

Figure 4.8 shows typical load–displacement responses from fracture tests of DCB specimens (Fig. 4.3) with the UF-B underfill having an as-manufactured fillet, reinforced fillet, and damaged fillet. For the as-manufactured and reinforced fillets, the maximum load indicates the crack initiation within the PCB. Observations of the specimen through an optical microscope showed that the first peak for the damaged fillet, point A in Fig. 4.8, corresponded to the onset of damage initiation at the interface between the solder mask and the first conducting layer of the PCB, while point B corresponded to crack arrest just before the first row of solder balls. The fillet reinforcement with the UF-B underfill increased the average fracture load by 28%, while removal of the fillet reduced the average strength by 33% compared with the as-manufactured fillet.

Depending on the fillet size, two different failure modes were observed, as indicated in Figs. 4.5 and 4.6, and schematically shown in Fig. 4.9. In the first mode, referred to as PCB cracking (PC), the crack initiated at the interface between the epoxy and glass fibers of the second, thicker prepreg layer (layer 7 in Table 4.2) and propagated along this path until the complete failure of the assembly. In the second failure mode, referred to as solder-mask cracking (SC), the crack initiated and grew at the interface between layer 1 (solder mask) and layer 2 (plated copper) of the PCB. In summary, both failure modes involved the cracking of an interface within the PCB, and in no case did the underfill itself crack.
Fig. 4.8 Representative load-displacement response of a DCB with the silica-filled underfill (UF-B) for different fillet sizes. Load given per unit specimen width.

Fig. 4.9. Crack paths (dashed lines) in the DCB specimens. a) Schematic cross-section of a DCB specimen, b) PCB cracking (PC), c) Solder mask cracking (SC)

Figure 4.10 gives the measured failure loads of the DCB specimens for the three types of fillet made with the filled and unfilled underfill adhesives as well as the cyanoacrylate adhesive.
Ten specimens of each underfill configuration were tested. The repeatability was good, with a standard deviation less than 10% in the maximum fracture force.

It is clear from Fig. 4.10 that the fracture load increased with the fillet size for both UF-A and UF-B. As mentioned previously, the failure mode in all cases was interlaminar delamination within the PCB; i.e. SC or PC. It is also evident from Figure 4.10 that the failure mode changed from SC to PC as the fillet size increased. This transition occurred sooner with UF-B, between the damaged fillet and the original fillet. Furthermore, since both underfills have the same fillet size in the damaged and reinforced configurations (Fig. 4.5(b-c) and Fig. 4.6 (b-c)), the difference between the average fracture loads for the unfilled and filled underfill adhesives was statistically insignificant at the 95% confidence level. However, as can be seen in Fig. 4.5(a) and 4.6(a), the size of the as-manufactured fillet for the filled underfill (UF-B) was larger than the fillet for the unfilled underfill (UF-A), and so fracture load was significantly larger for UF-B.

For the BGAs manually underfilled with the cyanoacrylate adhesive, CN, the failure mode was SC and the fracture load was similar to those of the damaged fillets of the BGAs with UF-A and UF-B (Figs. 4.5(c) and 4.6(c)). Since the CN underfill did not have a fillet, this is consistent with the hypothesis that the fracture load and the failure mode of underfilled BGAs under PCB bending depend only on the fillet size, and not the underfill properties.

Overall, these observations confirm that, regardless of the underfill thermal and mechanical properties, both the failure mode and the fracture load changed with the fillet size in a consistent way.
Fig. 4.10. Average crack initiation load per unit specimen width (N/mm) and mode of failure (PC: PCB cracking, SC: solder mask cracking) of the underfilled BGA-PCB assemblies with different fillet sizes, as shown in Figs. 4.5 and 4.6. Error bars show standard deviation, which was less than 10% in all cases.

For the filled and unfilled epoxies and cyanoacrylate tested here, the underfill-solder mask interface was always stronger than the solder mask-conducting layer interface. Therefore, failure was not related directly to the underfill, but rather was controlled by the strength of the bond between the solder mask layer and the first conducting layer of the PCB. This contradicts the view that the underfill layer is always the weakest link in a BGA-PCB assembly, and that the underfill coefficient of thermal expansion (CTE) and tensile modulus are primary factors affecting the mechanical strength of underfilled solder joints [5-9].

4.4.2. Fracture surface analysis and cracking sequence

Figure 4.11 shows scanning electron micrographs of the fracture surfaces on the PCB side and BGA side, respectively, for the PC failure mode. It is seen that the crack path was at the
interface between the glass fibers and the epoxy of the prepreg layer, layer 7 of Table 4.2. The EDX analysis shows the Si peak in Fig. 4.11(d) that was characteristic of the E-Glass fibers that contained mainly silica (SiO$_2$), alumina (Al$_2$O$_3$) and limestone (CaCO$_3$) [24].

Fig. 4.11. a) Fracture surface on the PCB side for the PC failure mode (crack path shown with dashed line in Fig. 4.9 (b)). The crack grew from left to right. (b-c) SEM micrographs of woven bundles of glass fibers (scale bars are 500 μm and 50 μm, respectively) (d) EDX spectrum of the fracture surface.

As shown in Figs. 4.5 and 4.6, the crack path in the PCB in both the SC and the PC failure modes consisted of two sections: a horizontal section corresponding to the transverse, interlaminar fracture of the PCB, and a vertical section corresponding to the tensile fracture of the PCB. The
horizontal section of the crack path initiated before the vertical section, as illustrated in Fig. 4.12, because the transverse tensile strength in fiber-reinforced polymeric composites is much smaller than the longitudinal tensile strength (parallel to the fibers) [14]. In fact, the transverse tensile strength is typically the same or somewhat smaller than that of the epoxy matrix between the fiber plies [16]. Similarly, the laminate structure of a multi-layer PCB is much weaker out-of-plane than in-plane [20]. Therefore, in the underfilled BGA-PCB assembly, the peel stresses at the ends of the BGA overlap induced transverse failure in the weak through-thickness direction. This is similar to the cracking of the surface epoxy that is commonly observed in structural adhesive joints with glass-epoxy adherends [25-29].

![Diagram](image)

**Fig. 4.12.** Cracking sequence in PCB failure in order of occurrence: a) crack initiation in horizontal direction (PCB transverse failure due to peel stresses), b) PCB cracking in vertical direction (PCB tensile failure), c) crack propagation in horizontal direction until final separation.

### 4.4.3. Comparison of stress fields

The maximum (first) principal stress has been shown to be a representative metric for crack initiation in adhesive joints [13-14]. The distribution of the first principal stress within the PCB close to the underfill fillet was calculated using the 2D FEM described in Section 4.3 and compared for BGA-PCB assemblies reinforced with UF-B for three different fillet sizes: no fillet, medium
fillet, large fillet (Fig. 4.13 (a-d)). The applied load to all these configurations was 1 N/mm. The no-fillet configuration, used to mimic the damaged fillet configurations, was modeled in two ways: (a) with a 90° corner, and (b) assuming a small degree of local rounding to avoid the stress singularity that results from a sharp corner [13-14]. Fig. 4.13 shows that the maximum principal stresses in the PCB always occurred at the interface between the underfill fillet and the PCB toward the inside of the underfill fillet. As expected, the maximum 1st principal stress of the no-fillet design with the sharp 90° corner (Fig. 4.13(a)) occurred at the singularity of the corner. In this case, unlike the other cases, the maximum stress was a function of the mesh density, increasing as the density increased. The maximum 1st principal stress of the no-fillet configuration with the slightly rounded corner (Fig. 4.13(b)) was approximately 45% smaller than that of the sharp corner. As expected from St. Venant's principle, comparison of Figs. 4.13(a) and 4.13(b) indicates that the corner modification changed only the local stress distribution, and that the stresses were approximately the same at a distance of about 20 μm from the nominal corner.

Figures 4.13(b-d) show that the maximum first principal stress decreased from 121 MPa to 61 MPa as the fillet became larger. This is consistent with the doubling of the failure load that was measured for the largest fillets of Fig. 4.10.

As discussed in Section 4.3, modelling the multi-layer PCB as an isotropic, homogeneous material produced a relatively small, systematic error in the stresses near the adhesive fillet. Another source of error was the neglect of the three-dimensional distribution of the copper traces within the PCB shown in Fig. 4.4. Therefore, the stresses reported in Fig. 4.13 were probably slightly different from the actual ones in areas where the distribution of copper traces was non-uniform. However, Fig. 4.4 shows that copper trace distribution within the first six layers (Table 4.2; depth of the crack in the reinforced fillet), in the vicinity of the fillet (dashed line of Fig. 4.4),
was relatively uniform, with only layer 4 (28 μm thick) showing considerable variation across the specimen. Nevertheless, since the internal structure was a constant in this work as the fillet geometry was changed, its neglect did not affect the comparison of the various underfill fillets. Therefore, the conclusion that larger fillets lead to lower stress concentrations, and to smaller maximum principal stresses was not affected.

![Diagram showing different fillet designs and their corresponding stress distributions.

**Fig. 4.13.** Distribution of first principal stress within the PCB close to the underfill fillet for three different fillet designs. The applied load was 1 N per mm of width. a) no-fillet with sharp corner, b) no fillet with slightly rounded corner (modified according to [13] to avoid singularity), c) medium fillet, d) large fillet.

The present modeling approach was not able to predict the detailed crack paths, because many of the interfacial bond strengths between the constituent materials (e.g. copper traces, epoxy, glass fibers, solder mask) were unknown, fabrication-induced residual stresses were neglected, and the heterogeneity of the internal structure of the PCB was ignored so that only global, average stresses were calculated.
At the moment of fracture, the 1 mm PCB arm of the DCB specimens (Fig. 4.3) showed much more deformation and curvature than the much stiffer 1.5 mm FR4 arm, although it remained elastic. Consequently, the loading on the underfill fillet and the crack propagation within the PCB was under mixed-mode conditions for both the SC and PC failure modes. However, the fillet size did not itself change the global mode ratio at the moment of crack initiation for the DCB configurations of Fig. 4.3. This is illustrated in Fig. 4.14 which shows that the direction of the first principal stress for different fillet sizes was unaffected by the fillet size over the range investigated. Therefore, the strength differences among the DCBs with different fillets were not attributable to changes in the mode ratio.

![Fig. 4.14 Direction of first principal stress near the underfill-PCB interface for three different fillet designs. The applied load was 1 N per mm of width.](image)

a) no-fillet, c) medium fillet, d) large fillet.

4.5. Conclusions

Fracture experiments with double cantilever beam (DCB) specimens were used to characterize the effect of fillet size on the strength of underfilled microelectronic BGA solder joints. Two commercial capillary epoxy underfills as well as a cyanoacrylate adhesive were tested
in order to investigate the effects over a broad range of thermal and mechanical properties of the underfill adhesives.

The underfill created a pronounced stress concentration in the printed circuit board (PCB) close to the underfill fillet, which led to crack initiation in the PCB. For relatively small fillets, or when there was no fillet, cracks initiated and propagated at the interface between the solder mask and the first conducting layer of the PCB. Larger fillets reduced the stress concentration at the solder mask-conducting layer interface, and shifted the crack to the glass fiber-epoxy interface of the prepreg layer within the PCB. This increased the crack initiation load by approximately 100% compared with the case where there was no fillet.

Regardless of the underfill properties or the fillet size, cracking always initiated within the PCB, indicating that the strength of the underfilled BGAs was independent of the strength of the underfill. Although this was observed over a broad range of underfill properties and a typical PCB and solder mask combination, the exact crack path may change for other combinations of materials having different bond strengths.

A finite element model showed that the maximum first principal stress in the PCB decreased as the fillet became larger, thereby explaining the measured increase in joint strength with fillet size. The global mode ratio at crack initiation within the PCB was unaffected by the underfill fillet size.

The results show that the underfill fillet size is an important design parameter which significantly influences the strength of underfilled solder joints. In summary, the larger the fillet the better; however, the size of the fillet is restricted by the dimensions of the BGA-PCB assembly.
4.6. References


Chapter 5

5. Bending strength of adhesive joints in microelectronic components: comparison of edge-bonding and underfilling

5.1. Introduction

In order to improve the mechanical reliability of microelectronic components and packages under drop, bending, and vibration, epoxy adhesives are frequently used to reinforce the connection between printed circuit boards (PCBs) and surface mounted components such as ball grid arrays (BGAs). Adhesives can be used as underfills [1-4] where a low-viscosity adhesive completely fills the gap between the component and the PCB, and edge-bonding [5-7] where only the perimeter of the component is bonded to the PCB using a more viscous adhesive. In underfilling, which is the more conventional approach, dots of a low-viscosity adhesive are dispensed on the printed circuit board near the periphery of the component where they flow through capillary action between the solder balls before being thermally cured [8-10].

Underfilling is also used to reduce thermal stresses in solder balls connecting a silicon die to a polymeric substrate within a surface mount component, and hence increase thermal fatigue life. These underfill adhesives typically contain fillers such as silica particles, which decreases the coefficient of thermal expansion and increases the Young’s modulus [11-14]. However, underfills between components and PCBs often do not contain fillers, which lowers the cost of manufacturing and processing [15].
In many BGA applications underfilling the entire area between the component and the circuit board is unnecessary and costs can be reduced by applying a more viscous adhesive only on the edges or corners of the component [16, 17]. This has the added advantage of making the components easier to replace and inspect [5].

PCB bending in BGA-PCB assemblies may result in cracking of either the adhesive or the substrates. In a previous study [18], the present authors showed that the size of an underfill fillet plays a major role in determining the joint strength, with larger fillets reducing the stress concentration at the PCB-fillet interface and strengthening the joint. Furthermore, over the broad range of underfill properties that were tested in [18], the joint bending strength remained only a function of the fillet size, and was independent of the underfill thermal and mechanical properties. All of the fillets in [18] were of similar concave shape and only their size was varied. The role of fillet shape was not studied.

Shi et al. [19] used three-point bending to test BGA-PCB assemblies edge-bonded with six different adhesives having tensile modulus varying between 0.67 GPa to 4.4 GPa, and two underfill adhesives with modulus of 3.08 GPa and 3.5 GPa. They reported that when using adhesives with a tensile modulus less than 3 GPa, the bending strength increased with the adhesive modulus, while it decreased with increasing modulus for adhesives with modulus greater than 3 GPa. They reasoned that the adhesives with higher modulus carried a larger share of the load and hence reduced the stresses in the solder joints. However, the stresses were not modelled and the effect of the adhesive fillet size was not considered. Shi et al. [20] also found that the thermal fatigue life of edge-bonded BGA-PCB assemblies, made with three adhesives having tensile modulus between 0.67 GPa to 7.0 GPa, improved significantly with higher modulus and lower coefficient of thermal expansion.
Qi et al. [21] compared a low modulus underfill with a high modulus underfill in three-point bending. The better performance of the low modulus underfill in bending was attributed to its ability to absorb more energy during deformation. The moduli values, however, were not reported.

A common technique to assess the drop performance of BGA-PCB assemblies is to mount them onto a rigid board and drop them repeatedly until failure, with the number of drops serving as a reliability index. Using this reliability index, Shi et al. [22] argued that adhesives with high modulus are more effective than the adhesives with low modulus in improving the reliability of BGAs under drop-impact loading.

Chang et al. [15] also compared the effect of an unfilled underfill epoxy and a silica-filled underfill epoxy on the drop performance of BGA-PCB assemblies, and found both underfills gave similar results. They did not suggest a reason for this and did not report the fillet size. They also found the filled underfill had a better performance in thermal fatigue.

Peng et al. [23] found that the drop performance of an unfilled commercial underfill was better than that of a filled underfill; however, they did not report the adhesive modulus or consider the effects of a varying fillet size.

It is evident that previous studies of the effect of underfilling and edge-bonding on the bending strength of BGA-PCB assemblies have produced conflicting results. Moreover, no previous studies have explained the measured performance differences in terms of the stresses leading to failure along the observed crack paths. There remains a need for a systematic study of the effects of underfill and edge-bond adhesive properties, fillet shape, and the effect of curing profile and residual stress on BGA-PCB bending strength.

The present paper examines the bending strength of underfilled and edge-bonded BGA-PCB assemblies using double cantilever beam (DCB) specimens. Two adhesives of each type were
considered while controlling the shape and size of the adhesive fillet, the degree of underfilling and the effect of thermal residual stress. The effect of each of these changes on the stress distribution in the PCB and the adhesive layer was investigated using finite element analysis and compared with the measured fracture loads and crack paths.

5.2. Experimental

5.2.1. Board assembly

The BGA-PCB assemblies used in this study were comprised of thin-profile fine-pitch ball grid array packages (iNAND Embedded Flash Drives, SanDisk, Milpitas, USA) with a trilayer structure containing a silicon chip interconnected to a bismaleimide-triazine (BT) substrate, and encapsulated in an epoxy molding compound (EMC). Table 5.1 gives the properties of these parts of the BGA-PCB assembly. These packages were soldered to 1 mm thick, multilayer PCBs consisting of insulating layers (woven glass fiber epoxy prepreg, FR4), conducting layers (plated copper), resin coated copper layers (RCC), and an epoxy solder-mask surface coating (Table 5.2). The substrate metallization on the PCB copper pads was organic solderability preservative (OSP).

The BGAs were soldered to the PCBs using a fully automated surface mount technology assembly line (BlackBerry, Cambridge, ON, Canada) which included production equipment for solder paste screen printing, package placement, and reflow soldering. After reflow, the solder ball diameter, height and pitch were 300 μm, 200 μm, and 500 μm, respectively. The gap between BGA and PCB was equal to the height of the solder balls after processing (200 μm), which is typical of many BGA components.

After solder reflow, underfilling or edge-bonding was performed. Two heat-cure low-viscosity adhesives were used for underfilling (UF-A, UF-B), while two room-temperature-cure viscous adhesives (EB-A, EB-B) were selected for edge-bonding (Table 5.3).
Table 5.1. Properties of the BGA-PCB assembly [4, 18]. Coordinate z is normal to the plane of the PCB.

<table>
<thead>
<tr>
<th>Material</th>
<th>Thickness (µm)</th>
<th>Young’s modulus (GPa)</th>
<th>Poisson’s ratio</th>
<th>Thermal expansion Coefficient (ppm/°C)</th>
</tr>
</thead>
<tbody>
<tr>
<td>BT</td>
<td>170</td>
<td>14.5</td>
<td>0.11</td>
<td>12</td>
</tr>
<tr>
<td>Silicon die</td>
<td>320</td>
<td>130</td>
<td>0.28</td>
<td>2.8</td>
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<tr>
<td>EMC</td>
<td>680</td>
<td>16.7</td>
<td>0.25</td>
<td>20</td>
</tr>
<tr>
<td>Solder</td>
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<td>0.4</td>
<td>21</td>
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<tr>
<td>PCB</td>
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<td>E_x=E_y=21.8</td>
<td>ν_x=ν_y=0.1</td>
<td>α_x=α_y=17</td>
</tr>
<tr>
<td></td>
<td></td>
<td>E_z=3.5</td>
<td>ν_z=0.25</td>
<td>α_z=60</td>
</tr>
</tbody>
</table>

Table 5.2. PCB layers (symmetric about layer 9, total number of layers=17), along with schematic of the PCB layup (not to scale). SM=solder mask, PL=plated copper, RCC=resin coated copper, PR=prepreg [18].

<table>
<thead>
<tr>
<th>Layer No.</th>
<th>1</th>
<th>2</th>
<th>3</th>
<th>4</th>
<th>5</th>
<th>6</th>
<th>7</th>
<th>8</th>
<th>9</th>
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<tr>
<td>Material</td>
<td>SM</td>
<td>PL</td>
<td>RCC</td>
<td>PL</td>
<td>PR</td>
<td>PL</td>
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<td>PL</td>
<td>PR</td>
</tr>
<tr>
<td>Thickness (µm)</td>
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<td>28.0</td>
<td>50.0</td>
<td>28.0</td>
<td>50.0</td>
<td>28.0</td>
<td>190</td>
<td>17.5</td>
<td>200</td>
</tr>
</tbody>
</table>
Table 5.3. Properties of adhesives as supplied by the manufacturer.

<table>
<thead>
<tr>
<th>Properties</th>
<th>Hysol UF3808 (UF-A)</th>
<th>Hysol UF3537 (UF-B)</th>
<th>Hysol E-40HT (EB-A)</th>
<th>Hysol E-60HP (EB-B)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Type</td>
<td>Epoxy</td>
<td>Epoxy</td>
<td>Epoxy</td>
<td>Epoxy</td>
</tr>
<tr>
<td>Tensile Modulus (GPa)</td>
<td>2.6</td>
<td>4.3</td>
<td>1.9</td>
<td>1.7</td>
</tr>
<tr>
<td>Viscosity (mPa.s)</td>
<td>360</td>
<td>4,000</td>
<td>16,000</td>
<td>25,000</td>
</tr>
<tr>
<td>CTE (ppm/°C)</td>
<td>55</td>
<td>47</td>
<td>60</td>
<td>50</td>
</tr>
<tr>
<td>Cure Schedule</td>
<td>5 min at 150°C</td>
<td>5 min at 150°C</td>
<td>24 hr at 22°C</td>
<td>24 h at 22°C</td>
</tr>
<tr>
<td>Filler weight fraction (wt%)</td>
<td>0</td>
<td>38</td>
<td>0</td>
<td>0</td>
</tr>
</tbody>
</table>

5.2.2. Underfill fillet size modifications

Most of the BGA-PCB assemblies were underfilled using an automated dispenser which applied a pre-determined volume of underfill to the edge of each BGA package where it flowed to fill the gap between the BGA and the PCB as well as between the solder balls. The board was then transferred to an oven for the cure. Heating further reduced the adhesive viscosity and facilitated its flow under the component. Some underfilled specimens were made manually in order to eliminate the adhesive that flowed between the BGA and the PCB, as described in Section 5.2.3.

In an earlier study [18], it was shown that larger adhesive fillets in underfilled specimens can strengthen a BGA-PCB assembly under PCB bending. In the present study, the original fillet size of the underfilled BGAs created on the assembly line was enlarged to ensure that all underfilled and edge-bonded specimens had the same fillet size and shape. To enlarge an existing underfill fillet and create a consistent fillet size, extra underfill of the same type was dispensed manually on the PCB along the edge of the BGA package and cured as the original underfill (UF-A or UF-B in Table 5.3). Figure 5.1 shows an example of a BGA-PCB assembly reinforced with
UF-B along with the average fillet length, height, and radius of curvature. In this figure, the interior layer of the fillet shows the original UF-B underfill fillet created in the SMT line, while the exterior layer shows the added UF-B fillet in the secondary manual process. Small differences in the color of the interior and exterior layers of the underfill fillet were attributable to differences in the lighting and digital color rendering during micrography.

![Reinforced fillet in a BGA-PCB assembly underfilled with UF-B. Dimensions in mm.](image)

**Fig. 5.1.** Reinforced fillet in a BGA-PCB assembly underfilled with UF-B. Dimensions in mm.

### 5.2.3. Edge-bonding: Effect of fillet shape, underfilling and residual stress

A number of specimens were edge-bonded manually using EB-A or EB-B. In order to evaluate the effect of the adhesive fillet curvature on joint strength in edge-bonded specimens, specimens were made with three different fillet shapes: concave fillet, straight fillet, and convex fillet (Fig. 5.2). Since viscous adhesives do not flow easily, their fillet shape could be easily modified to create different fillet shapes. However, because of the low viscosity of the underfill adhesives, all underfilled specimens had a concave fillet (Fig. 5.1).

Polytetrafluoroethylene (PTFE) or Teflon rods and shims were used to control the fillet shape and create straight and concave fillets in edge-bonded specimens (Fig. 5.3). The diameter of the rod used to create the concave fillet (3.5 mm) was twice the average radius of curvature of the fillet in underfilled specimens (Fig. 5.1) to ensure the concave configuration of the edge-bonded
specimens had the same fillet size and shape as the underfilled specimens. Convex fillets were generated by dispensing more adhesive on the PCB near the BGA edge.

![Diagram](image)

**Fig. 5.2.** Three different adhesive fillet shapes considered in this study for edge-bonded specimens, a) concave fillet, b) straight fillet, c) convex fillet.

![Diagram](image)

**Fig. 5.3.** Schematic of edge-bonding process with a viscous adhesive. The fillet shape was controlled using PTFE rods and inclined flats. Fillet height was 1.10 mm as in Fig. 5.1. Dimensions in mm. Not to scale.

In edge-bonded specimens, as shown in Figs. 5.2-5.3, a small volume of the adhesive flowed underneath the BGA, but it did not extend to the solder balls. The length of this adhesive layer under the BGA depended on the adhesive viscosity, the surface tension of the adhesive and the adjoining surfaces, and the pressure applied by the PTFE rod or shim. Microscopic inspection of 5 specimens of each adhesive type indicated that the average length of the edge-bond adhesive layer underneath the BGA (the underfill layer) was 1.12 mm for EB-A and 1.63 mm for EB-B when either a PTFE rod or inclined flat was used, and 0.35 mm for EB-A and 0.60 mm for EB-B
when no PTFE rod or shim was used. Since the distance between the BGA edge and the first solder ball was 2.6 mm (Fig. 5.3), the adhesive layer did not extend to the solder balls.

It was of interest to separate the effects of the two regions of an underfilled or edge-bonded adhesive joint; i.e. the fillet and the adhesive layer between the BGA and the PCB. To examine this, a number of underfilled and edge-bonded specimens were made with a thin PTFE film inserted underneath the BGA to exclude the adhesive (Fig. 5.4). The underfill or edge-bond adhesive was then applied manually and cured to generate the fillet.

![Fig. 5.4. a) A BGA-PCB assembly underfilled with UF-A, b) bonding layer underneath the BGA was replaced with a PTFE film.](image)

As shown in Table 5.3, UF-A and UF-B were heat-cure epoxies, and EB-A and EB-B were room-temperature-cure. A higher cure temperature may result in a larger thermal residual stress in the adhesive layer, which may change the stress state in the PCB. In order to investigate the effect of the cure temperature on the strength and failure mode of a BGA-PCB assembly, a number of specimens were edge-bonded with EB-A and cured using alternative cure schedules of 2 h at either 60°C, 80°C, or 100°C. As with the room-temperature cured edge-bonded specimens, the fillet shape in these new specimens was controlled using a Teflon rod (Fig. 5.3). Visual inspection and measurement using an optical microscope showed that curing at the elevated temperature did not create any detectable change in the fillet shape.
The specimens were placed inside a pre-heated oven and were left to cool inside the closed oven. The resulting time-temperature profile of the adhesive was recorded with a thermocouple embedded in the adhesive layer of a number of calibration specimens cured at different temperatures.

### 5.2.4. DCB fracture specimens

The fracture specimens shown in Fig. 5.5, were cut from the underfilled and edge-bonded BGA-PCB assemblies, described in Sections 5.2.2 and 5.2.3, using a saw with a diamond blade. The free surface of each BGA package was first sanded using a 400-grit sponge sander, then wiped with acetone, and finally bonded to a 1.5 mm thick loading arm made from another PCB (FR4 type IS410, Isola, Chandler, Arizona, USA). To ensure the FR4-BGA joint was stronger than the BGA-PCB joint, a room-temperature curing, toughened structural epoxy adhesive (Hysol E-40HT) was used to bond the BGA to the FR4 loading arm. A cyanoacrylate adhesive (Loctite 496) was used to bond the brass loading brackets to the ends of PCB and FR4 loading arms. The specimens were then fracture tested at a constant cross-head speed of 1.5 mm/min using a tensile testing machine. For each configuration of underfilled or edge-bonded DCB specimens (Figs. 5.5a-b), ten specimens were tested. Five specimens were tested for each of the other modified configurations described in Sections 5.2.2 and 5.2.3.
Fig. 5.5. (a-b) Schematic of underfilled and edge-bonded DCB specimens, respectively, showing the bonded FR4 loading arms and the loading brackets. Not to scale. (c) A photograph of a DCB specimen before bonding the loading brackets. Dimensions in mm. Specimen width was 8 mm.

Figure 5.6 shows the distribution of copper traces in the PCB. The plated copper layers in the PCB were not uniform over the area of the BGA, but were patterned according to the signal processing requirements of the BGA. As discussed in Section 5.4, these patterned layers could be approximated as being homogeneous with respect to the stresses and strain energy release rates that governed fracture within the PCB and the underfill and edge-bond adhesives.
Fig. 5.6. a) Top view of a BGA-PCB assembly after cutting and before bonding the FR4 arm, b) the distribution of copper traces in layers 2, 16, c) layers 4, 14, and d) layers 6, 8, 10, 12 of Table 5.2. The white areas between the copper traces (green) were filled with the laminate epoxy. The dashed line shows the transverse line of crack initiation from the toe of the underfill fillet [18].

5.2.5. Fracture energies for underfill adhesives and PCB

Three different types of DCB specimens were fabricated and tested in order to make a relative comparison of the size of the initiation critical strain energy release rate for each of the crack paths observed within the BGA-PCB DCB specimens. The objective of these additional DCB experiments was to compare the fracture energies along three crack paths from a uniform starting condition, chosen here to be that created by a folded piece of aluminum foil.

The crack initiation strain energy release rate, $G_{ci}$, of the UF-A and UF-B underfills was measured with DCB specimens of bonded AA6061-T6 aluminum alloy, as shown in Fig. 5.7. Prior to the application of the underfill, the adherends were pretreated using the $P_2$-etch method [24], which included mechanical abrasion, acetone degreasing and sulphuric acid etching to remove the existing aluminum oxide layer. Crack initiation was facilitated by embedding a folded aluminum
foil within the adhesive layer [25]. After the application of the underfill, the aluminum adherends were clamped together against two 127 µm steel wires to maintain the desired underfill thickness. The small amount of the underfill that had flowed from the sides of specimen was removed using a grit 400 sand paper. The recommended curing profile of at least 5 min at 150°C was monitored using a thermocouple embedded in the adhesive layer. The DCB specimens were loaded in a tensile testing machine at a constant crosshead speed of 1 mm/min until a critical load was reached when crack extension occurred from the tip of the folder foil.

![Diagram of DCB specimen](image)

**Fig. 5.7.** DCB specimen for measurement of underfill $G_{ci}$. Dimensions in mm. Not to scale.

The $G_{ci}$ of the interface between the solder mask and the PL of the PCB (layer 2 in Table 5.2) was measured using the DCB of Fig. 5.8, bonded with the underfill adhesive UF-A. Since the adhesion strength between the UF-A and solder mask was greater than that between the solder mask and the PL layer, the crack initiated at the interface between the solder mask and the PL layer. As explained in Section 5.4.2.1. and shown in Fig. 5.6, at the location of crack initiation for solder mask cracking, there were no copper traces in the PL layer (layer 2 in Table 5.2). Therefore, in this failure mode, a crack propagated at the interface between the solder mask and the epoxy of the PL layer. To generate a similar crack path in PCB-UF-PCB specimens, the folded aluminum foil that facilitated the crack initiation was placed in a position where there were no copper traces.
The PCB adherends of Fig. 5.8 were degreased with acetone, and two 127 μm diameter steel wires were used to control the thickness of the underfill layer when the specimen was clamped together during cure. After curing, excess underfill that had flowed from the specimen edges was removed using a grit 400 sand paper, and two brass brackets were bonded to the adherends using a cyanoacrylate adhesive (Loctite 496).

![Diagram](image)

**Fig. 5.8.** DCB specimen for measurement of $G_{ci}$ of solder mask PCB interfacial fracture and PCB prepreg fracture. Dimensions in mm. Not to scale.

The initiation strain energy release rate ($G_{ci}$) of the PCB in the prepreg layer was measured using a similar DCB specimen, but the PCB surface was first sanded using a 400 grit sand paper to remove the solder mask, and then cleaned with acetone. This shifted the crack path into the PCB at prepreg layer 7 (Table 5.2).

Five specimens were tested for each DCB configuration. As explained in Section 5.4.2.2, the load-displacement curve was linear to failure in all DCB specimens, and no crack extension was observed before the attainment of the maximum load, which was followed by rapid fracture. Therefore, the crack initiation strain energy release rate ($G_{ci}$) of the underfills and the PCB were determined using these maximum loads as inputs to a finite element model following [26, 27].

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250 μm long crack was modelled using singular elements in the mid-plane of the underfill or within the PCB, and $G_{ci}$ was calculated using the $J$-integral method.

5.3. Finite element analysis (FEA)

The stresses in the various underfilled and edge-bonded BGA-PCB specimens of Fig. 5.5 were calculated using ANSYS™ finite element software under linear elastic conditions. Plane 183 elements were used throughout in plane strain. The results were insensitive to this assumption, with the maximum first principal stress in the PCB changing by only 1%, and in the adhesive fillet by only 3% when plane stress was assumed instead.

The stress state in the adhesive layer as well as in the PCB in the immediate vicinity of the adhesive fillet was investigated for different fillet geometries. It is known that stresses predicted by finite element models of adhesive joints are highly sensitive to the element size, particularly at sharp corners between two different materials [28-31]. This can be circumvented by rounding the corners [28, 29]. However, the stress will depend to some extent on the degree of rounding.

The mesh was progressively increased from a 10 μm element size in the immediate vicinity of the singular points (e.g. the end of the fillet or the corner of the BGA) to a 50 μm element size for regions distant from the singular points. The sensitivity of the results to mesh density in both regions was checked using a finer mesh.

In the present work, since the primary interest was in comparing the distributions of the first principal stress rather than its magnitude, the artifacts of sharp corners were minimized by considering the stresses a small distance from the corner, following the approach of Grant et al. [30]. In the present case they were obtained at nodal points one element away from the sharp corners; i.e. 10 μm from the interface.
The PCB was modelled as a homogeneous orthotropic material with the average properties of Table 5.1. Therefore, as mentioned in Section 5.2.4, the heterogeneity of the internal structure of the PCB resulting from the three-dimensional distribution of the copper traces within the PCB was ignored. This was justified by the close spacing of the traces and the focus of the FEA on the role of the adhesive fillet size, shape and mechanical properties in determining the bending strength of the BGA-PCB assemblies. Moreover, as explained in Section 5.4.2.1, the main failure modes of solder mask cracking and PCB cracking occurred at interfaces where there were no copper traces; i.e. between layers 1 and 2, and along the glass fibers of layer 7 (Table 5.2). As discussed in the next section, the trends predicted by these nominal stresses were in good agreement with the measured fracture loads as a function of the fillet parameters.

5.4. Results and discussion

The predictions of the FEA are presented in Section 5.4.1 in order to explain the trends seen in the stress distributions as a function of the type of adhesive, the fillet geometry, and whether it was an underfill or an edge bond. The experimental results are then discussed and explained in terms of the predicted stress distributions in Section 5.4.2.

5.4.1. PCB and fillet stress distributions

Figure 5.9 shows the FEA predictions of the first principal stress distribution in the PCB, one element away from the PCB-fillet interface, for the underfilled and edge-bonded DCB specimens (configurations of Fig. 5.5). In the edge-bonded specimen, it was assumed there was no adhesive in the gap between the BGA and the PCB. Since the primary interest was in the distribution of the first principal stress rather than its magnitude, the force applied to both arms of the DCB in the finite element model was arbitrarily set as $F = 1$ N/mm.
As expected, the stress was maximum near the singularity at the toe end of the fillet. The close agreement between the underfilled and edge-bonded configurations shows that the stresses generated by the adhesive fillet were insensitive to the presence of the underfill adhesive layer between the PCB and the BGA. Therefore, the length of this underfilled region could be ignored as a variable in the experiments and modeling in the cases where failure began in the region of the toe of the fillet.

![Graph showing PCB 1st principal stress](image)

**Fig. 5.9.** FEA predictions of PCB 1st principal stress, one element below the PCB-fillet interface, for the underfilled and edge-bonded configurations of the specimen of Fig. 5.5 with applied load $F=1$ N/mm of specimen width. $x=0$ corresponds to the toe end of the fillet.

Although the underfill adhesive in the gap between the PCB and BGA had a negligible effect on the PCB stress distribution at the toe of the fillet (Fig. 5.9), it did affect the stress in the adhesive near the sharp corner of the BGA, as shown in Fig. 10. The stress at this point (A) was a function of the local degree of mesh refinement, and so it was evaluated one element away from the interface along the dashed line in Fig. 5.10.
Figure 5.10 shows that lack of any adhesive between the PCB and BGA in the idealized edge-bonded case increased the maximum first principal stress in the fillet near the singular corner (Point A) by 60%. However, as discussed in Section 5.2, in all edge-bonded specimens there always existed a short length of adhesive between the BGA and the PCB (Fig. 5.5).

Figure 5.11 shows that the maximum first principal stress in the adhesive fillet one element away from point A was reduced to the value of a completely underfilled joint when the underfill length of the edge-bonded BGA was as small as about $L=0.2$ mm. Therefore, as long as the singular sharp corner (Point A in Fig. 5.10) was surrounded by the adhesive, the stress concentration at the fillet-BGA interface was largely avoided.

**Fig. 5.10.** FEA predictions of the 1st principal stress across the fillet, one element from the BGA corner for the underfilled and idealized edge-bonded configurations of the DCB specimen of Fig. 5.5 with applied load $F=1$ N/mm of specimen width.
Fig. 5.11. FEA predictions of maximum 1st principal stress one element from corner A versus the underfill layer length in the edge-bonded DCB specimen (Fig. 5.5b).

Applied load was $F=1$ N/mm of specimen width.

Figure 5.12 shows that an increase of the underfill modulus from $E=2$ GPa to $E=5$ GPa, increased the maximum first principal stress in the PCB by just 5%. Therefore, the differences in the modulus of the underfill and edge-bond adhesives (Table 5.3) had a negligible effect on differences in the observed failure loads and crack paths.

Figure 5.13 shows the effect of fillet curvature on the first principal stress distribution in the PCB for concave, straight, and convex fillets, with the height and length specified in Fig. 5.1. It is seen that the concave fillet produced a more uniform distribution of the maximum first principal stress with a maximum that was 22% smaller than that for convex fillet. These trends were a function of the angle of contact between the adhesive and the PCB, so that the straight fillet produced an intermediate distribution and maximum first principal stress.
Fig. 5.12. FEA predictions of PCB 1st principal stress, one element away from the PCB-fillet interface, for different values of underfill Young’s modulus for the underfilled DCB specimen of Fig. 5.5a. $x=0$ corresponds to the toe end of the fillet.

Applied load was $F=1$ N/mm of specimen width.

Fig. 5.13. Effect of adhesive fillet curvature on 1st principal stress distribution in the PCB, one element away from the PCB-fillet interface, for the underfilled configuration of Fig. 5.5a. $x=0$ corresponds to the toe end of the fillet.

Applied load was 1 N/mm of specimen width.
The present underfill adhesives were heat-cured, while the edge bonds were made with room-temperature cure adhesives. Therefore, when comparing them it was of interest to assess the effect of cure temperature and its role in generating thermal residual stresses. For this purpose, a fillet of adhesive type EB-A (Table 5.3) was modelled as it cooled from its cure temperature where crosslinking was completed. The initial state of the BGA-PCB assembly in the finite element model was assumed to be stress free and fully cured. Figure 5.14 shows that the transverse tensile stress (y-direction) introduced in the PCB for cure temperatures of 60, 80, and 100°C was maximum approximately 150 μm beneath the PCB surface and increased with cure temperature. This trend was quite insensitive to the PCB transverse properties; e.g. the depth of the maximum stress changed only 5% for a 100% change in the transverse coefficient of thermal expansion of the PCB. As will be seen in the experimental results, such transverse tensile stress may contribute to delamination within the PCB and cause a shift in the crack path.

**Fig. 5.14.** Transverse tensile stress in the y-direction induced in the PCB during the cooling phase of the adhesive cure for different cure temperatures for the edge-bonded configuration of Fig. 5.5b.
5.4.2. Fracture loads and failure modes

5.4.2.1. Failure modes

Overall, three different failure modes were observed in underfilled and edge-bonded specimens, as shown schematically in Fig. 5.15. In the PCB cracking (PC) failure mode, the crack initiated at the interface between the glass fibers and the epoxy in prepreg layer 7 (Table 5.2) and grew along this path until the final fracture of the specimen (Fig. 5.15a and Fig. 5.16a). This failure mode was observed in both the underfilled specimens (Figs. 5.1, 5.4a, and 5.5a) and the edge-bonded specimens (Fig. 5.5b) that were cured at 100°C, but not in those edge-bonded specimens that were cured at lower temperatures.

Fig. 5.15. Side views of different failure modes and crack paths (dashed lines) found in the DCB specimens of Figs. 5.4-5.5. a) PCB cracking (PC), b) solder-mask cracking (SC), and c) adhesive cracking (AC).
Fig. 5.16. Fracture surfaces on PCB and the corresponding profilometer scan for the failure modes of: a) PC (PCB cracking), and b) SC (solder mask cracking).

In the solder mask cracking (SC) failure mode, the crack initiated and propagated quickly along the interface between the solder mask (layer 1 of Table 5.2) and the epoxy region of the plated copper (layer 2, Fig. 5.6) (Fig. 5.15b and Fig. 5.16b). The crack then arrested just before the first row of solder balls, and the measured load increased before the crack again propagated very quickly and the package and solder balls failed completely. In about 70% of the cases this secondary crack path was predominantly IMC cracking with some pad cratering (i.e. cracking through the PCB surface epoxy under the copper pads; Fig. 5.16b). In almost 30 percent of the specimens, the secondary crack was deeper, propagating along layer 7 of PCB as in the PC failure mode. The SC failure mode was observed in the edge-bonded specimens (Fig. 5.5b) cured at room temperature as well as those cured at 60°C and 80°C. It was not seen in any of the underfilled joints.
In the adhesive cracking (AC) failure mode, the crack propagated at the vertical interface between the adhesive fillet and the BGA (Fig. 5.15c). It occurred only when there was no adhesive in the gap between the PCB and BGA so that the adhesive fillet alone bonded the component to the board (Fig. 5.4b).

The fracture surfaces were scanned with an optical profilometer (ST400, Nanovea, Irvine, USA) to measure the thickness of the cracked PCB layer for the failure modes of PC and SC. Figure 5.16a shows that the PC failure mode was characterized by the appearance of the woven bundles of glass fibers, and that the average depth of the crack was approximately 250 μm, confirming that the crack propagated in layer 7 (Table 5.2). Figure 5.16b shows that in the SC failure mode cracks grew at a depth equal to the thickness of the solder mask, at the interface between the solder mask (blue) and the surface epoxy (dark green) of layer 2 of the PCB.

### 5.4.2.2. \( G_{ci} \) for underfill adhesives and PCB

The critical strain energy release rate for crack initiation, \( G_{ci} \), was measured for three crack paths: (i) cohesive cracking within the two underfill adhesives using the specimen of Fig. 5.7, (ii) interfacial cracking between the solder mask and layer 2 of the PCB using the specimen of Fig. 5.8; and (iii) delamination cracking within the PCB using a modification of the specimen of Fig. 5.8. As shown in Fig. 5.17, the force-displacement curves of both the Al and PCB DCB specimens were linear to the moment of fracture when the crack propagated quickly in an unstable fashion, reflecting the relatively brittle nature of the underfills and the PCB. All the Al-UF-Al DCB specimens failed cohesively in the adhesive. The crack propagation in the BGA-PCB DCB specimens was similar to that of the PCB-UF-PCB specimens.
Fig. 5.17. Load vs. crosshead displacement up to crack extension for a) AL-UF-AL DCB specimens with unfilled underfill (UF-A) and silica-filled underfill (UF-B), configuration of Fig. 5.7, b) UF-PCB-UF DCB specimens with configuration of Fig. 5.8.

SC: solder mask cracking, PC: PCB cracking
The overlap length in the Al DCB specimens was 60 mm (Fig. 5.7). The crack initiated at the maximum load and instantly propagated along the entire joint. Similarly, in the PCB-UF-PCB DCB specimens, crack initiation corresponded to the maximum load. However, the crack arrested after a few millimeters of rapid, unstable growth as the load dropped. The measured load then increased as the cross-head advanced until the crack propagated again for several millimeters. This pattern continued along the overlap length (20 mm, Fig. 5.8) until the final fracture of the joint, giving rise to the jagged shape of the load-displacement curve of Fig. 5.17b.

Figure 5.18a shows the intact PCB surface, and Fig. 5.18b shows the PCB surface after a fracture test of a PCB-UF-PCB DCB specimen, revealing the SC failure mode characterized by the appearance of the PCB surface epoxy, similar to Fig. 5.16b. In the PCB-UF-PCB DCB specimens, which originally had no solder mask, the failure mode was PC, similar to Fig. 5.16a.

**Fig. 5.18.** PCB surface before and after fracture test of PCB-UF-PCB DCB specimens. The failure mode was SC. a) Intact PCB surface, b) PCB surface after fracture test.

Although the Al adherends in Al-UF-Al specimens were much stiffer than the PCB adherends in PCB-UF-PCB specimens, it is known that the $G_{ci}$ dependence on adherend stiffness is relatively weak provided that the adherends deform only elastically [25]. The absence of any residual curvature on the adherends and finite element modeling confirmed that the adherends
remained completely elastic in both Al-UF-Al and PCB-UF-PCB DCB specimens. Therefore, the
$G_{ci}$ data shown in Fig. 5.19 can be considered to be characteristic of the observed crack paths. The
average $G_{ci}$ values were 393 J/m$^2$ and 632 J/m$^2$ for adhesives UF-A and UF-B, respectively. The
$G_{ci}$ for the SC and PC failure modes were much smaller, explaining why cracks initiated in the SC
and PC modes rather than in the adhesive layer of the BGA-PCB assemblies. It was not necessary
to measure $G_{ci}$ of the edge-bond adhesives (EB-A and EB-B), since they were toughened structural
epoxy adhesives which are much stronger than underfill adhesives [25].

![Graph showing $G_{ci}$ values](image)

**Fig. 5.19.** $G_{ci}$ values in mode I calculated by the FEM for the cohesive failure of UF-A and UF-
B, and also for the failure modes of the SC and PC. Error bars represent ±1 standard
deviation based on 5 repeat experiments for each failure mode.

### 5.4.2.3. Fracture of specimens with concave fillets

The load–displacement responses for the DCB fracture tests of underfilled and edge-bonded
BGA-PCB assemblies with concave fillets (configurations of Fig. 5.5) were linear to the maximum
load corresponding to crack initiation in the PCB, followed by rapid, unstable crack growth within
the PCB and the rupture of the specimen.
Figure 5.20 shows the failure mode and the average fracture load of these DCB specimens (Fig. 5.5a-b) with a concave fillet of the same size and shape (Fig. 5.1) for the four types of adhesive. The differences in the fracture loads were statistically insignificant ($t$-test, 95% confidence) even though the crack path for the underfills was within the PCB (PC failure mode), while for the edge-bonds it was at the interface between the solder mask and layer 2 (Table 5.2), SC failure mode. This is consistent with the PCB stress distribution for underfilled and edge-bonded specimens (Fig. 5.9), and also with the insensitivity of the PCB stress to the adhesive Young’s modulus (Fig. 5.12). Since in both the SC and PC failure modes, cracks initiated within the PCB, adhesive properties had a negligible effect on the fracture load and failure mode. In no case did the adhesive layer itself crack, which is consistent with $G_c$ data (Fig. 5.19), showing that the cohesive failure of underfills and toughened edge-bond adhesives requires a significantly higher energy than the PCB interfacial failure.

![Figure 5.20](image)

**Fig. 5.20.** Average fracture load per unit specimen width and corresponding failure mode (PC: PCB cracking, and SC: solder mask cracking) of BGA-PCB specimens with a concave fillet (Fig. 5.1 and Fig. 5.2a) underfilled and edge-bonded with different adhesives (Table 5.3). Error bars correspond to ±1 standard deviation based on 10 specimens for each adhesive.
These results are also consistent with [18], where it was shown that the bending strength of underfilled joints was only a function of the fillet size, which was held constant for the DCBs of Fig. 5.5. They are also consistent with previous studies [16, 19, 32] using three-point and four-point bending experiments of underfilled and edge-bonded BGAs that have shown that the edge of the adhesive joint creates a significant stress concentration and is the most likely location for damage initiation.

5.4.2.4. Effect of residual curing stress

Although the underfilled and edge-bonded specimens with concave fillets had identical strength, the failure modes were different; i.e. SC in edge-bonded specimens and PC in underfilled ones. The fillet shapes in underfilled and edge-bonded specimens were very similar in size and shape (Fig. 5.1, 5.3, and 5.4). Therefore, the difference in the failure modes was attributed to the thermal residual stresses predicted by the FEA (Fig. 5.14) as a result of the elevated-temperature cure of the adhesives used in the edge-bonded specimens. This hypothesis was examined by curing three sets of five edge-bonded specimens of EB-A at 60, 80, 100°C for 2 h instead of at the normal room-temperature cure. The objective was to determine whether the generation of thermal residual stress could change the crack path in the edge-bonded specimens. All of these specimens had essentially the same fracture load of 4.8 N/mm; differences were statistically insignificant at the 95% confidence level. In the edge-bonded specimens cured at 60°C or 80°C, the failure mode remained unchanged at SC. However, in the specimens cured at 100°C the failure mode shifted to PC in 4 of the 5 specimens, remaining as SC in one case. This supported the hypothesis that the difference in the failure modes of the underfilled and edge-bonded specimens in Fig. 5.20 were due to the thermal residual stresses created in the heat-cured underfill adhesives and their absence in the room-temperature cured edge-bond adhesives.
5.4.2.5. Effect of fillet shape

Figure 5.21 shows that the fracture load decreased as the fillet radius of curvature and its contact angle with the PCB surface increased for edge-bonded specimens with EB-A. A pair-wise comparison among the three cases indicated that the strength differences were statistically significant (95% confidence level). This trend can be attributed to the increase in the stress concentration at the end of the fillet that was predicted by the FEA for the straight and convex fillets compared with the curved fillet (Fig. 5.13). A more abrupt change of the fillet slope and a larger contact angle between the PCB and the adhesive fillet raises the stress concentration at the PCB-fillet interface significantly. The relative values of the largest 1\textsuperscript{st} principal stresses in the PCB predicted for each of the fillet configurations correlated well with the relative strengths associated with different fillet shapes. For example, the maximum 1\textsuperscript{st} principal stress increased by 28% from \textasciitilde60 MPa for the concave fillet to \textasciitilde77 MPa for the convex fillet (Fig. 5.13), consistent with the 27% reduction in the failure load of the convex fillet (Fig. 5.21).
Fig. 5.21. Effect of adhesive fillet curvature on the average fracture load of DCB specimens (Fig. 5.5b) edge-bonded with EB-A. All specimens failed in the SC mode (interface between solder mask and PCB layer 2). Five specimens were tested for each fillet shape.

Error bars show ±1 standard deviation.

5.4.2.6. Effect of adhesive in gap between PCB and BGA

For five specimens of each underfill or edge-bond adhesive, a PTFE film was inserted between the BGA and PCB before adhesive cure, as shown in Fig. 5.4b, to prevent adhesive from flowing underneath the BGA, thereby creating an idealized edge-bonded specimen where only the adhesive fillet bonded the BGA to the board. Figure 5.22 shows the measured fracture loads when this was done for both the underfill and edge-bond adhesives of Table 5.3, along with the data from Fig. 5.20 for underfilled or edge-bonded specimens in which there was some adhesive between the PCB and BGA.

The failure mode in the specimens with no adhesive between the PCB and the BGA was AC (Fig. 5.15c), with the crack propagating along the vertical interface between the fillet and BGA. This is consistent with the FEA prediction of the location of maximum first principal stress near the corner of the BGA (Point A in Figs. 5.10-5.11). This was the only failure mode in which crack
initiated at an interface within the adhesive layer. This failure mode disappeared when even a very small amount of adhesive entered the gap between the PCB and BGA, consistent with the large decrease in this corner stress predicted by the FEA for underfill lengths of more than about 200 \( \mu \text{m} \), as seen in Fig. 5.11.

Since the failure mode of AC, unlike the SC and PC, involves the cracking of an interface within the adhesive, adhesive properties were important and influenced the failure load. This explains the difference in the fracture load for different adhesives in Fig. 5.22.

![Figure 5.22](image.png)

**Fig. 5.22.** Average fracture load per unit width and corresponding failure mode of DCB specimens with different underfill and edge-bond adhesives, in which there is no adhesive in the gap between the BGA and PCB (Fig. 5.4b), along with the data from Fig. 5.20 (dash lines) for the specimens fully or partially underfilled. Error bars correspond to +/- 1 standard deviation based on 5 specimens for each case.
5.5. Conclusions

Fracture tests of underfilled and edge-bonded BGA-PCB specimens showed that, as long as the adhesive fillet size and shape was the same, edge-bonding was as effective as underfilling in improving the bending performance of a BGA-PCB assembly. In all specimens, cracks initiated and grew within the PCB. These results were consistent with finite element modeling that showed that the distribution of the first principal stress in the PCB near the PCB-fillet interface was almost identical in underfilled and edge-bonded configurations.

The predominant failure mode in the underfilled specimens was PCB subsurface cracking in a prepreg layer, and in the edge-bonded specimens it was cracking under the solder mask layer of the PCB. This difference in failure modes was due to the different cure temperature of the underfilled and edge-bonded specimens which affected the thermal residual stresses.

The bending strength of edge-bonded specimens increased with the adhesive fillet curvature, consistent with the trends for the variation in the first principal stress predicted by finite element analysis.

These experiments and modeling showed that the local fillet shape and size at the end of the adhesive layer had a far more important effect on joint bending strength than the adhesive thermal and mechanical properties and the bonding method (i.e. underfilling vs. edge-bonding). For this reason, the edge-bonded and underfilled BGAs with the same concave fillet size had identical strength.
5.6. References


[21] Qi F, Ding Y, Ding Z, Fu H. Reliability of ball grid array (BGA) assembly with reworkable capillary underfill material. In 6th International Conference on Electronic Packaging Technology 2005 Sep 2 (pp. 1-7). IEEE.


Chapter 6

6. Predicting delamination in multilayer composite circuit boards with bonded microelectronic components

6.1. Introduction

A printed circuit board (PCB) electrically connects and mechanically supports microelectronic components such as resistors and ball grid array (BGA) packages. Multilayer PCBs are composite laminates consisting of woven glass-fiber epoxy insulating layers and conducting copper layers bonded together under heat and pressure. The copper layers are etched to create the required pattern of conducting traces, and the epoxy of the adjacent insulating layers fills the spaces where the copper was removed, thereby creating copper–epoxy composite conducting layers [1]. Other sites of potential delamination result when an adhesive is used to fill the gap between the underside of a component such as a BGA and the surface of the PCB. The bonds of such “underfill” adhesives act to reinforce the connection with the PCB [2-4].

Interlaminar damage or delamination is one of the main failure modes in PCB composite laminates, because they are much weaker out-of-plane than in-plane. PCB delamination may initiate during the soldering process, particularly with lead-free solders which require higher reflow temperatures than leaded solders [5]. Bending during installation, service and repair can also cause PCB delamination [2, 4]. Furthermore, thinner PCBs can be more susceptible to warpage deformation and hence delamination [6] under both thermal and mechanical loading. Therefore, it is important to understand the delamination mechanism and measure the relevant
properties governing the process. Cohesive zone models (CZMs) are often used to simulate crack initiation and propagation in both adhesively-bonded joints [7-11] and composite laminates [12-16]. Double cantilever beam (DCB) specimens are frequently used to study delamination and determine the cohesive law parameters in composites laminates [17-19].

Fuchs and Fellner [20] used a CZM to simulate the fracture of mode I DCB specimens made of 0.65, 2, and 5 mm thick PCB adherends. The test specimens were laminated woven glass-fiber prepreg layers containing a Teflon film to form a 20 mm pre-crack. The cohesive parameters were obtained from the 5 mm thick DCBs, and the model was validated by comparing the measured and predicted fracture loads for the other mode-I DCBs. There was a good agreement between the experimental and simulation results. However, the model was not tested for bending delamination in actual circuit boards containing copper layers and microelectronic components.

Schoengrundner et al. [21] used four-point bending to obtain the cohesive parameters for the interface between the prepreg and copper layers in a multilayer PCB. However, the model was not used to make predictions of PCB delamination.

Multilayer PCBs are not homogeneous through their thickness, with conducting and insulating layers of different properties bonded at interfaces that may have different strengths as reflected by the critical strain energy release rate, $G_c$. In a previous study [22], the present authors showed that underfilled BGA-PCB assemblies under bending always failed in the PCB, and the location of the onset of delamination interface changed with the size of the spew fillet of the underfill epoxy adhesive, and the subsequent crack path followed different interfaces in the multilayer PCBs.

The main aim of the present work was to develop a CZM to predict the initiation and growth of delamination in realistic underfilled BGA-PCB assemblies tested under different bending
conditions. Since PCB delamination was the dominant failure mode, the traction-separation parameters of the CZM were obtained from fracture tests of test specimens made from PCBs bonded with the underfill epoxy, without any microelectronic components. The two-parameter CZM was coupled with finite element analysis (FEA) to simulate the delamination and progressive failure in the BGA-PCB assemblies.

6.2. Experimental methods

Some of the data for the present modelling were obtained from the fracture experiments of DCB specimens (PCB-underfill (UF)-PCB) of the present authors in ref. [23]. Also, the fabrication of the BGA-PCB specimens tested in the present study was very similar to ref. [22]; therefore only a summary of these materials and procedures is presented here. The new experiments using various BGA-PCB bending specimens, aimed at determining and predicting the effect of underfill fillet size, PCB stiffness and loading rate, are described in greater detail in Section 6.2.2.

6.2.1. Board assembly

BGA packages (iNAND Embedded Flash Drives, SanDisk, Milpitas, USA) with dimensions of 16 ×12 × 0.85 mm having 153 solder balls were assembled on a 1 mm thick PCB in a surface mount technology (SMT) development line (BlackBerry, Cambridge, ON, Canada) (Figs. 6.1a-b). The solder paste applied on the PCB was SAC305 (Indium, New York, USA). The distance between the first row of solder balls and the BGA edge was 2.6 mm. The PCB layup is shown in Fig. 6.1c and Table 6.1. The thicknesses and material properties of the different constituents of the BGA-PCB assembly are given in Table 6.2.

After solder reflow, the BGA-PCB assemblies were underfilled with a low viscosity epoxy adhesive (Hysol UF3808, Henkel Electronic Materials, Irvine, CA, USA) that was automatically
dispensed in a predetermined volume to the edge of each package where capillary action caused the underfill to flow under the BGA package and fill the entire gap between the BGA, PCB, and solder balls. It was then cured at 150°C for 5 min.

Fig. 6.1. a) PCB surface with copper pads before package assembly [22]. b) BGA packages mounted on PCB. The test specimens were prepared by cutting the BGA-PCB assembly along dashed lines. Dimensions in mm [22]. c) Schematic of the PCB cross-section revealing different conducting and insulating layers. SM=solder mask, PL=plated copper, RCC=resin coated copper, PR=prepreg [23].
Table 6.1. PCB layup (total number of layers=17, symmetric about layer 9). SM=solder mask, PL=plated copper, RCC=resin coated copper, PR=prepreg [22].

<table>
<thead>
<tr>
<th>Layer No.</th>
<th>1</th>
<th>2</th>
<th>3</th>
<th>4</th>
<th>5</th>
<th>6</th>
<th>7</th>
<th>8</th>
<th>9</th>
</tr>
</thead>
<tbody>
<tr>
<td>Material</td>
<td>SM</td>
<td>PL</td>
<td>RCC</td>
<td>PL</td>
<td>PR</td>
<td>PL</td>
<td>PR</td>
<td>PL</td>
<td>PR</td>
</tr>
<tr>
<td>Thickness (μm)</td>
<td>20.0</td>
<td>28.0</td>
<td>50.0</td>
<td>28.0</td>
<td>50.0</td>
<td>28.0</td>
<td>190</td>
<td>17.5</td>
<td>200</td>
</tr>
</tbody>
</table>

Table 6.2. Properties of the BGA-PCB assembly [22, 24].

<table>
<thead>
<tr>
<th>Material</th>
<th>Thickness (μm)</th>
<th>Young’s modulus (GPa)</th>
<th>Poisson’s ratio</th>
</tr>
</thead>
<tbody>
<tr>
<td>BT substrate</td>
<td>170</td>
<td>14.5</td>
<td>0.11</td>
</tr>
<tr>
<td>Silicon die</td>
<td>320</td>
<td>130</td>
<td>0.28</td>
</tr>
<tr>
<td>EMC</td>
<td>680</td>
<td>16.7</td>
<td>0.25</td>
</tr>
<tr>
<td>Solder</td>
<td>200</td>
<td>51</td>
<td>0.4</td>
</tr>
<tr>
<td>PCB</td>
<td>1000</td>
<td>$E_x=E_y=21.8$</td>
<td>$E_z=3.5$</td>
</tr>
<tr>
<td></td>
<td></td>
<td>$\nu_{xz}=\nu_{yz}=0.1$</td>
<td>$\nu_{xy}=0.25$</td>
</tr>
<tr>
<td>Underfill</td>
<td>200</td>
<td>2.6</td>
<td>0.35</td>
</tr>
</tbody>
</table>

During the underfilling process, a fillet was formed at the edge of each BGA package (Fig. 6.2a). The size of the as-manufactured underfill fillet was a function of the underfill viscosity and the surface tension of the underfill and the adjoining surfaces. The quasi-static fracture tests of these BGA-PCB assemblies in [22] showed that a larger underfill fillet increased the bending strength. This was studied in detail in [22] by increasing the fillet size in certain BGA-PCB DCB specimens by adding underfill to the as-manufactured fillet as shown in Fig. 6.2b. In this study, the effect of the fillet size was investigated using the bending specimens described in Section 6.2.2.
Fig. 6.2. Cross-sections of underfilled BGA-PCB assemblies showing the different spew fillets of the underfill epoxy adhesive. a) as-manufactured fillet, b) reinforced fillet.

The in-plane Young’s modulus and tensile strength of the PCB were measured in [22] as 21.8 GPa and 254 MPa, respectively (5 specimens, 5% standard deviation, ASTM D3039). Since the PCB mostly consisted of relatively brittle glass fibers and epoxy resin, the PCB tensile behavior was linear until fracture [22]. The out-of-plane ($z$ direction) properties of the PCB given in Table 6.2 were obtained from ref. [24].

6.2.2. BGA-PCB fracture specimens

In this study, fracture specimens were prepared from the BGA-PCB assemblies and were tested in the bending configurations of Fig. 6.3 in order to investigate the effect of underfill fillet size, PCB stiffness and strain rate on the failure mode and fracture load.

The BGA-PCB assemblies were cut using a diamond saw blade as indicated by the dashed line in Fig. 6.1b. The free surface of the BGA was sanded using a 400-grit sand paper, cleaned with acetone, and then was bonded to a rigid support. The specimen of Fig. 6.3a was made with an as-manufactured and a large fillet as described above. The specimens of Figs. 6.3b and 6.3c were made with the as-manufactured fillet only. To study the effect of the PCB stiffness on the fracture load and failure mode, in some specimens the PCB was reinforced by bonding a second PCB to it (Fig. 6.3b) using a cyanoacrylate adhesive (Loctite 496).
Fig. 6.3. Underfilled BGA-PCB bending test specimens with different configurations, a) quasi-static test, b) quasi-static test with two bonded PCBs, c) drop-impact test.

The specimens of Fig. 6.3a and 6.3b were tested at a constant cross-head speed of 1.5 mm/min, and the applied load was measured using a 200 N load cell. The impact specimens of Fig. 6.3c were tested using the drop tester of ref. [25] with a release height of 192 mm, which generated a strain rate of 8 s⁻¹ at the underfill fillet, as calculated using the finite element model of Section 6.4. Five specimens were tested for each of the quasi-static loading configurations (Fig. 6.3a-b) and for the drop-impact loading (Fig. 6.3c).
The fracture load in both the quasi-static and drop-impact tests was measured using a 3 mm strain gauge (SGD-3/120-LY13, Omega, USA) bonded to the PCB surface opposite to the underfill fillet, as shown in Fig. 6.3. A finite element analysis showed that this location provided the maximum sensitivity to the onset of delamination. The strains were recorded using a high-speed data acquisition system with a sampling frequency of 10 Hz for quasi-static tests and 150 kHz for drop-impact tests.

6.2.3. Determination of cohesive parameters for PCB delamination

Two different failure modes were observed in the bending tests of Fig. 6.3, similar to those seen with DCB specimens of ref. [22]. The first failure mode was crack propagation at the interface between the solder mask (layer 1 in Table 6.1) and the first conducting layer (layer 2). This failure mode was called SC (solder mask cracking) in ref. [22]. In the second failure mode a crack grew at the interface between the glass fibers and epoxy of one of the prepreg layers (layer 7). This failure mode was named PC (PCB cracking) [22]. Further details of these failure modes are presented in Section 6.3.

The following sections explain the procedures used to determine the CZM parameters for each crack path (SC or PC) under both quasi-static and drop-impact loadings.

6.2.3.1. Quasi-static loading

The mode I interlaminar strain energy release rate, $G_{nc}$, for both failure modes (SC and PC, Fig. 6.3) were measured in [23] using quasi-static tests of the PCB-UF-PCB DCB specimens shown in Fig. 6.4 with an underfill bondline thickness of 127 μm. The DCB failure mode was always SC when the solder mask was present on the surface of the PCB, and PC when the solder mask layer was removed from the PCB surface using a 400 grit sand paper. In the present study, the quasi-static cohesive strengths for both failure modes were obtained by comparing the
experimental load-displacement data from these DCB tests of [23] (Fig. 6.4) with the CZM predictions. This process is explained in greater detail in Section 6.4.3.1. These cohesive strengths and the $G_{nc}$ values from [23] were then used in the quasi-static CZM described in Section 6.4.1 to simulate the fracture of the present bending BGA-PCB specimens described in Section 6.2.2 and shown in Fig. 6.3.

Fig. 6.4. DCB specimen used in [23] to determine quasi-static $G_{nc}$ for both SC and PC failure modes. Dimensions in mm. Not to scale.

6.2.3.2. Drop-impact loading

The CZM parameters for the PC failure mode at high strain rate were obtained from a drop-impact test illustrated in Fig. 6.5. The fabrication process of this specimen was very similar to the DCB specimens of Fig. 6.4, except that the PCB arm was bonded to a rigid support. As discussed in Section 6.5, the PCB failure mode under drop-impact loading was always PC. Therefore, the configuration of Fig. 6.5 was used to extract the cohesive parameters of the PC failure mode at high strain rate in a process that was very similar to that was used for the quasi-static parameters using the DCB specimen of Fig. 6.4, and explained in Section 6.4.3.2.
Since the failure mode was always PC in the drop-impact test, it was not possible to use the specimen of Fig. 6.5 to determine the CZM parameters for the SC failure mode at high strain rate. For this reason, an analytical method was used to find the SC cohesive parameters at high strain rate using their quasi-static values. Several studies have shown that the cohesive strength and fracture energy of adhesive joints has the following dependence on strain rate [26-29]:

$$P_u(\dot{\varepsilon}) = P_u(\dot{\varepsilon}_0) \left(1 + m \ln \frac{\dot{\varepsilon}}{\dot{\varepsilon}_0}\right)$$

(1)

where $P_u(\dot{\varepsilon})$ is the rate-dependent cohesive parameter, $P_u(\dot{\varepsilon}_0)$ is the reference cohesive parameter under quasi-static loading, and $m$ is an experimentally-determined parameter typically greater than 0.1. This equation is used in Section 6.4.3 to determine the CZM parameters for the SC failure mode at high strain rates.

6.3. Experimental results

6.3.1. Failure modes and cracking sequence

The fracture tests of [23] using the PCB-UF-PCB DCB specimens (Fig. 6.4) showed that the average $G_{nc}$ for the SC and PC failure modes was 180 and 230 J/m², respectively. These values were smaller than the quasi-static $G_{nc}$ for the cohesive failure of the underfill, which was measured in [23] using aluminum-UF-aluminum DCB fracture specimens. For this reason, the underfilled
BGA-PCB assemblies of Fig. 6.3 always failed in the PCB, and in no case did the underfill layer itself crack.

Depending on the underfill fillet size, strain rate, and PCB stiffness, the crack grew either within the layer 7, referred to as PCB cracking (PC), or at the interface between layer 1 (epoxy solder mask) and layer 2 of the PCB (Table 6.1), referred to as solder mask cracking (SC). Both the SC and PC failure modes are schematically shown in Fig. 6.6. As explained in ref. [23], at the location of crack initiation in the SC failure mode there were no copper traces in layer 2, and it was evident that cracks grew at the interface between the solder mask and the epoxy of layer 2. Therefore, crack propagation in the SC failure mode was analogous to cohesive fracture of an epoxy adhesive layer in an adhesively-bonded joint.

![Diagram of PCB delamination failure modes](image)

**Fig. 6.6.** PCB delamination failure modes observed in BGA-PCB assemblies. a) Cross-section of a BGA-PCB DCB specimen, b) PCB cracking (PC), c) solder mask cracking (SC) [22].

The cracking sequence of the PCB involved two competing failure mechanisms with the sequences shown in Fig. 6.7: delamination at either the SC or PC interfaces, which was simulated based on a CZM in Section 6.4.1, and the subsequent rupture of the PCB cracked layer, modeled according to the maximum stress criterion in Section 6.4.2.
Since the out-of-plane tensile strength of the PCB was much lower than its in-plane tensile strength, the failure started as a delamination at an interface in layer 7 (PC failure mode) or between layers 1 and 2 (SC failure mode) (Fig. 6.7a). To simulate this interface delamination, cohesive zone elements behaving according to a traction-separation law were defined along the interface. Details of the CZM and the corresponding FEM are explained in Section 6.4.1.

After the PCB started to delaminate in either the PC or SC modes, stress became concentrated in the PCB cracked ligament until it exceeded the ligament tensile strength (Fig. 6.7b) which was modelled using a maximum stress criterion as explained in Section 6.4.2. Finally, the original delamination propagated to the end of the PCB (Fig.6.7c).

![Fig. 6.7.](image)

**Fig. 6.7.** Cracking sequence in the multilayer PCB in order of occurrence: a) delamination initiation in horizontal direction at either PCB layer 7 (PC failure mode) or at the interface between layers 1 and 2 (SM failure mode), b) PCB rupture in vertical direction due to tensile stresses, c) delamination propagation in horizontal direction [22].

It should be noted that the thickness of the cracked (delaminated) layer (Fig. 6.7a) was quite different in the SC and PC failure modes (25 µm in SC and 250 µm in PC, as reported in [23]). According to the FEM predictions presented in Section 6.5, in the SC failure mode the delamination initiation in the PCB (Fig 6.7a) and the rupture of the cracked layer (Fig. 6.7b) occurred almost simultaneously, because the thickness of the cracked layer in the SC failure mode
was so small relative to the PCB overall thickness (1 mm). However, in the FEM of the PC failure mode, which occurred during quasi-static loading of the bending specimen (Fig. 6.3a) with a large fillet (Fig. 6.2b) as well as in drop-impact loading (Fig. 6.3c), the initial delamination and the tensile rupture of the thicker cracked layer were distinct events. This will be further explained in Section 6.5.

6.3.2. Load displacement data

Figure 6.8 shows representative measured load-displacement data of the PCB-UF-PCB DCB specimens (Fig. 6.4) for both the SC and PC failure modes under quasi-static loading. The maximum load corresponded to delamination initiation (Fig. 6.7a) and the rupture of the delaminated layer (Fig. 6.7b), which effectively took place simultaneously. The curve was almost linear until fracture, showing that the PCB had little or no plastic deformation before damage initiation. The FEM predictions of the load-displacement data are also presented in Fig. 6.8. These will be discussed in Section 6.4.3.1.
Fig. 6.8. Load—displacement curves for representative DCB joints of Fig. 6.4 under quasi-static loading: a) SC failure mode, b) PC failure mode. The curves predicted by the FEM will be discussed in Section 6.4.3.1.
6.4. Finite element analysis

A 2D finite element model (FEM) with 4-node plane-strain structural elements (Plane 182, ANSYS®15, Ansys Inc, Canonsburg, PA) was used to model the BGA-PCB specimens (Fig. 6.3) or PCB calibration specimens (Figs. 6.4-6.5). The mechanical behavior of the model was assumed to be isotropic linear elastic, except the PCB which was modeled as an orthotropic linear elastic material (Table 6.2). This was justified by the experimental observations that there was no residual curvature in the PCB arms of the failed bending test specimens of Fig. 6.3, and the load-displacement curves of all specimens were effectively linear until fracture (Fig. 6.8). The boundary conditions considered in the FEM were consistent with the fixation of the DCB and bending specimens (Figs. 6.3-6.5); i.e. the free surface of the BGA package in the bending specimen (Fig. 6.3) was fixed in all directions, while the right edge of the DCB specimens (Fig. 6.4) was fixed in both the horizontal and vertical directions; also, the upper surface of the adhesive layer in the bending specimen of Fig. 6.5 was fixed in all directions.

The mechanical properties used in the FEM of the BGA-PCB and PCB specimens are listed in Table 6.2. The element size was changed smoothly from 25 μm in the vicinity of the cohesive interface to 100 μm in the substrates. A convergence study showed that the results were independent of the element size over this range (Fig. 6.9).
Fig. 6.9. Finite element mesh of the specimen of Fig. 6.4 near the crack initiation point for the PC failure mode, a) before load application, b) delamination initiation, c) rupture of the PCB cracked layer and delamination growth.

6.4.1. PCB delamination: Cohesive zone modeling

The finite element model described in Section 6.4 was used to simulate the delamination process in the PCB. A mixed-mode CZM was used to maintain the equilibrium of the shear and tensile stresses, but delamination was assumed to be governed by only the mode I CZM response. This assumption was found to provide an accurate model of solder cracking in ref. [30] using the same type of BGA-PCB assemblies as used here, but without an underfill adhesive. This behavior was attributed to the flexibility of the present PCB which prevented the generation of appreciable mode II loading [30, 31] during crack initiation and propagation. This assumption greatly simplified the task of specifying the CZM parameters.
Several studies have shown that, when the fracture is dominated by the mode I component of loading, the form of the traction–separation curve has less importance than the value of the cohesive parameters [32-34]. For this reason, a bilinear traction-separation law was selected for crack growth analysis (Fig. 6.10). In the bilinear model it is assumed that with increase of the load the cohesive stress linearly increases to a maximum value ($\sigma_{nc}$). At this critical stress softening and damage initiates at the interface. As the damage grows, the cohesive stress decreases until separation reaches a critical value ($\delta_{nc}$) where cohesive stress vanishes and delamination initiation takes place with the elements bounding the interface separating completely. The area under the traction separation curve was set equal to the critical strain-energy release rate, $G_{nc}$, as measured using the DCB specimen of Fig. 6.4 for quasi-static loading, and using the bending specimen of Fig. 6.5 as well as Eq. (1) for impact loading.

![Bilinear Traction–Separation Law](image)

**Fig. 6.10.** The bilinear traction–separation law used to model PCB delamination in the FEM.

The cohesive zone modeling procedure for the calibration PCB specimens (Figs. 6.4 and 6.5) and the actual BGA-PCB bending specimens (Fig. 6.3) was identical. The thickness of the cracked PCB layer in the SC and PC failure modes was approximately 25 µm and 250 µm, respectively [23]. Both the SC and PC interfaces were modeled using surface-to-surface contact elements that
behaved according to the bilinear traction-separation law of Fig. 6.10. The contact surface on one side of the cohesive interface was defined with the 3-node CONTA172 elements, while the target surface on the other side was defined with TARGE169 elements. Switching the surfaces selected for these contact and target had no effect on the results.

It has been shown that the initial stiffness of the interface, $K_n$, has a negligible effect on the failure load predictions [35]. For this reason and to be consistent with [30] and [36], default stiffness in ANSYS ($K_n = 10^{14} \text{N/m}^3$) was used.

An iterative procedure was employed to find the cohesive strength, $\sigma_{nc}$, that provided the best match between the simulation results in the calibration PCB specimens (Figs. 6.4-6.5) and the experimental load–displacement data.

**6.4.2. PCB rupture: Maximum stress criterion**

In the FEM, the load was incrementally increased until delamination was predicted to occur in the PCB along the designated cohesive interface shown in Figs. 6.7a and 6.9b. As illustrated in Fig. 6.7b, after some delamination in the horizontal direction, the axial stress due to bending in the delaminated PCB layer was large enough to lead to PCB rupture along the path A-B in Fig. 6.9a. If this second failure mechanism was not modeled in the FEM, the predicted load behavior was incorrect, with the load dropping slightly after delamination initiation, and then increasing again continuously, because the PCB delaminated layer continued to carry the load as the delamination propagated.

In order for the FEM to be able to accurately describe the experimental force displacement curve, the rupture of the PCB cracked layer along path A-B was simulated by joining nodes on the two sides of the path A-B with a constraint relation. When the PCB tensile strength was reached,
all constrained nodes were released to simulate the PCB rupture along A-B. As a result, the load dropped (Fig. 6.8), and the final phase of delamination began (Figs. 6.7c and 6.9c).

6.4.3. **CZM calibration**

For quasi-static loading, the CZM was calibrated for both the SC and PC failure modes using the load displacement curves obtained from the PCB-UF-PCB DCB specimens of [23] (Fig. 6.4). For drop-impact loading, the parameters of the CZM for the PC failure mode were obtained from a PCB drop-impact test (Fig. 6.5), while for the SC failure mode, an analytical equation was used (Eq. (1)) because the SC mode could not be induced at high strain rates. This greatly reduced the cost and time required to fabricate the calibration specimens, because they did not include any microelectronic component. Details of the extraction of the cohesive parameters from these test data are explained in the following sections.

6.4.3.1. **Quasi-static loading: PCB-UF-PCB DCBs**

Figure 6.8 compares the experimental data for the quasi-static fracture test of the PCB-UF-PCB DCB specimens (Fig. 6.4) with the simulation results for both the SC and PC failure modes. The cohesive strength, $\sigma_n$, was adjusted for each failure mode so that the peak load in the FEM was equal to that of the experimental curve. Fig. 6.8 shows that the modelling strategy described in Sections 6.4.1 and 6.4.2 was well able to capture the experimental load displacement curve.

The cohesive parameters of the SC and PC interfaces are presented in Table 6.3 for quasi-static as well as drop-impact loading. As discussed in Section 6.2.3.2, $\sigma_{nc}$ and $G_{nc}$ for drop-impact loading were obtained using a PCB bending specimen as well as a semi-logarithmic equation relating cohesive strength and toughness with strain rate.
Table 6.3. CZM parameters for the PC and SC failure modes.

<table>
<thead>
<tr>
<th>Failure mode</th>
<th>Loading type</th>
<th>$K_n$ (N/m$^3$)</th>
<th>$\sigma_{nc}$ (MPa)</th>
<th>$G_{nc}$ (J/m$^2$)</th>
</tr>
</thead>
<tbody>
<tr>
<td>PC</td>
<td>Quasi-static</td>
<td>$10^{14}$</td>
<td>4.8</td>
<td>230</td>
</tr>
<tr>
<td>PC</td>
<td>Drop-impact</td>
<td>$10^{14}$</td>
<td>5.9</td>
<td>390</td>
</tr>
<tr>
<td>SC</td>
<td>Quasi-static</td>
<td>$10^{14}$</td>
<td>16.1</td>
<td>180</td>
</tr>
<tr>
<td>SC</td>
<td>Drop-impact</td>
<td>$10^{14}$</td>
<td>33</td>
<td>363</td>
</tr>
</tbody>
</table>

An interesting observation was that under both quasi-static loading and drop-impact the cohesive strength for the SC mode ($\sigma_{nc}=16.1$ and 33 MPa) was significantly higher than that of the PC mode ($\sigma_{nc}=4.8$ and 5.9 MPa). This may be related to the similarity of the SC failure mode, crack propagation between the epoxy-based solder mask (layer 1) and the epoxy of the copper-containing layer 2, and the failure of an adhesive joint. Blackman et al. [33] have shown that in the cohesive zone simulation of adhesively-bonded joints, the value of $\sigma_{nc}$ is close to the tensile yield strength of the bulk adhesive. However, the value of $\sigma_{nc}$ for cohesive zone simulation of delamination in the fiber-reinforced composite laminates was significantly smaller than the transverse tensile strength of the laminate [33]; therefore, $\sigma_{nc}$ does not have a clear physical interpretation in cohesive zone modeling of delamination in composite laminates.

6.4.3.2. Drop-impact loading: PCB bending specimen and an analytical solution

The bending configuration of Fig. 6.5 was used to determine the cohesive parameters of the PC failure mode under impact loading conditions. As in Section 6.4.3.1, the cohesive strength was adjusted to ensure the predicted peak load in the FEM was equal to the measured maximum load. The high strain-rate cohesive parameters for the PC failure mode are given in Table 6.3.

As explained in Section 6.2.3.2, both $\sigma_{nc}$ and $G_{nc}$ for the SC failure mode under drop-impact loading were calculated using Eq. (1). This was because the drop impact specimen always failed
in the PC mode and so it was not possible to obtain a direct measurement of the fracture loads for the SC delamination. Figure 6.11 schematically shows the variation of a CZM with strain rate as suggested by Eq. (1). The average strain rate at the underfill fillet for underfilled BGA-PCB assemblies, tested under quasi-static (Fig. 6.3a) and drop-impact (Fig. 6.3c) bending loading conditions, were 0.0003 s\(^{-1}\) and 8 s\(^{-1}\), respectively, as calculated using a finite element model. Table 6.3 shows that the cohesive strength for the SC mode for quasi-static loading was \(\sigma_{nc} = 16.1\) MPa, and the fracture energy was \(G_{nc} = 180\). Assuming a value of 0.1 for the parameter \(m\) in Eq. (1) for drop-impact loading (Fig. 6.3c) gave \(\sigma_{nc} = 33\) and \(G_{nc} = 363\), which represented a strengthening of the SC mode at higher strain rates. As will be discussed in Section 6.5.1.3, this change in the cohesive parameters in the FEM shifted the crack path from the SC to the weaker PC mode at high strain rate in agreement with the experimental observations. Assuming a value greater than 0.1 for \(m\) unnecessarily increased the values of the \(\sigma_{nc}\) and \(G_{nc}\) further, rendering the SC interface even stronger.

![Fig. 6.11. Effect of strain rate on a bilinear CZM [37] showing increase in peak cohesive strength and fracture energy with increasing strain rate as per Eq. (1).](image)

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6.5. Comparison of FEM predictions and experimental measurements

In order to show the capability of the proposed CZM to study both the delamination initiation and propagation in the multilayer PCBs assembled with microelectronic components, the model predictions are compared with the experimental data for underfilled BGA-PCB assemblies in Section 6.5.1, which discusses the effect of strain rate, fillet size, and PCB stiffness on the load-displacement behavior and failure mode. A summary of the average fracture loads of different specimens is presented and discussed in Section 6.5.2.

6.5.1. CZM verification: Underfilled BGA-PCB assemblies

This section discusses the ability of the proposed model to predict the load displacement behavior and the crack paths observed in the underfilled BGA-PCB bending specimens tested with different strain rates, PCB stiffnesses and underfill fillet sizes (Figs. 6.2 and 6.3).

To demonstrate the capability of the model to predict arbitrary crack trajectories, both the SC and PC interfaces were modeled in parallel in a single FEM, with the corresponding CZM parameters (Table 6.3). The load was increased incrementally, until delamination was predicted to initiate and propagate in either the SC or PC modes, whichever was predicted to fail first.

6.5.1.1. Effect of underfill fillet size – quasi-static loading

Figure 6.12 compares the measured load-displacement curves for small and large fillets with those predicted using the traction-separation relations of Table 6.3 in the finite element model described in Section 6.4. The FEM correctly predicted the SC failure mode with the small fillet and the PC mode for the large fillet (Fig. 6.2), as shown in Fig. 6.13. As explained in [22], the smaller fillet produced a higher stress concentration at the toe of the underfill fillet, which caused the highest stresses to occur between layer 1 and 2 and so initiate the SC failure mode. Figure 6.12
also shows good agreement in the behavior to the maximum load and the subsequent unloading to the point where the crack arrested at the first row of solder balls. After this, however, the experimental results of Fig. 6.12a show that the load increased again until crack extended to the next row of balls, sometimes through the solder and sometimes under it within the PCB [23]. Since only the crack initiation load (the maximum load) was of interest, the crack arrest and subsequent propagation was not modeled.

Figures 6.12 and 6.13 demonstrate the important role of the fillet size on both the failure mode and fracture load. The average maximum measured load for the large fillet was 1.7 times larger than the corresponding value for the small fillet. Five specimens were tested for each fillet size, with standard deviation of less than 10% for each fillet size.
Fig. 6.12. Representative curves of measured and predicted load per unit width as a function of displacement for BGA-PCB specimens of Fig. 6.3a with two different fillet sizes (Fig. 6.2): a) small fillet, b) large fillet.
Fig. 6.13. FEM predictions of the effect of the underfill fillet size on the location of the delamination initiation within the PCB. Delamination initiated at the SC interface for relatively small fillets, and at the PC interface for large fillets.

In the FEM for the small fillet, the peak load corresponded to the SC mode delamination initiation and the simultaneous rupture of the PCB. For the large fillet, the PC delamination initiation in the FEM was marked by a small nonlinearity in the rising slope of the load-displacement curve near the peak (Point A in Fig. 6.12b), which resulted from the abrupt change in the PCB stiffness after crack initiation. This nonlinearity was not seen in the experimental curve, probably because of the smooth transition from damage initiation to the loading of the PCB ligament (path A-B in Fig. 6.9). The predicted load then increased after delamination initiation until the PCB tensile strength in the ligament was reached and the associated nodes (path A-B in Fig. 6.9) were released, thereby decreasing the load sharply.

6.5.1.2. Effect of PCB stiffness – quasi-static loading

Figure 6.14 compares the experimental and FEM load-displacement curves for the PCB substrate reinforced by bonding a second PCB to it using a cyanoacrylate adhesive (Fig. 6.3b). It is seen that the fracture load corresponding to initiation and subsequent fracture increased by more than 120% when a second PCB was bonded to the original PCB. The FEM predicted the fracture
load and the subsequent drop in load with a reasonable accuracy; the predicted fracture load was 14% larger than the average measured value. However, the FEM predicted the PC failure mode whereas it was SC in the experiments. This discrepancy was attributed to the modelling of the PCB as a homogeneous material, thereby neglecting the effect of the different mechanical properties of the various layers (glass fiber, epoxy, and copper) on local stress concentration within the PCB. These details were of greater importance in the reinforced (stiffer) PCBs, probably because the higher fracture loads in these specimens created a larger stress concentration at the interfaces of different materials and layers. A more accurate prediction of the failure mode and fracture load requires a more detailed model taking into account the heterogeneity of the PCB structure. As in Fig. 6.13, Fig. 6.14 shows that the load increased once again after the crack arrested at the first row of solder balls, and that the FEM was not designed to model this subsequent crack growth beyond the solder balls.

![Graph](image)

**Fig. 6.14.** Representative load-displacement curve measured for the BGA-PCB specimen with two PCBs, and FEM prediction (Fig. 6.3b).
6.5.1.3. Effect of strain rate

In quasi-static bending tests (Figs. 6.3a and b), the applied force was measured using a load cell, while in the drop-impact tests (Fig. 6.3c), the PCB deformation was measured using a strain gauge and the loads were calculated using the FEM. The accuracy of this procedure is illustrated in Figure 6.15 which compares the measured force-strain curve for a quasi-static loading of the specimen of Fig. 6.3a with the finite element results. No crack or damage was modeled, and linear and nonlinear geometric solutions were considered in the FEM. Both the experimental data and the FEM results showed an almost linear relationship between force and strain. Since the 1 mm PCB had a low bending stiffness, the nonlinear solution had a better agreement with the experimental data. The experimental force-strain curve had an average slope that was 4% smaller than the nonlinear FEM prediction, probably because the strain gauge was bonded to the PCB surface (Fig. 6.3a), which was coated with an epoxy solder mask with a tensile modulus an order of magnitude smaller than the average in-plane modulus of the PCB (Table 6.2). However, in the FEM, the PCB was modeled as a homogeneous material with the average properties of Table 6.2.

Approximately, 77% of the volume fraction of the PCB consisted of $E$-glass fibers and a brittle epoxy, with the balance being copper. Since the elastic properties of both the fibers and this epoxy can be assumed to be only weak functions of strain rate [38, 39], the force strain curve of Fig. 6.15 (FEM, nonlinear geometry) was used to obtain the failure load from the maximum measured strain in the drop-impact tests.
Fig. 6.15. Measured and predicted force-strain graph of bending specimen of Fig. 6.3a before crack initiation.

The bending specimens were tested under quasi-static (Fig. 6.3a) and drop-impact conditions (Fig. 6.3c) to assess the effect of the strain rate on the fracture load and failure mode. As explained in Section 6.5.1.1, under quasi-static loading, the failure mode was SC, while Fig. 6.16 shows that it was PC under drop-impact loading.

Fig. 6.16. Effect of strain rate on the failure mode of BGA-PCB assemblies under bending: a) SC mode in quasi-static loading (Fig. 6.3a), b) PC mode in drop-impact test (Fig. 6.3c).
A typical strain-time graph of the drop-impact test is shown in Fig. 6.17. The peak load (largest negative strain) corresponded to crack initiation in the PCB, followed by a reduction in the load as the crack propagated. The oscillations seen in the second half of the graph were due to the vibration of the PCB after complete separation, with a frequency that was very close to the mode I natural frequency of the PCB arm fixed as a cantilever beam.

**Fig. 6.17.** Strain-time graph for a typical drop-impact test (Fig. 6.3c).

Figure 6.18 compares the experimental and simulation results of the strain-displacement curve for a representative drop-impact test. Knowing the impact velocity from the initial height of the striker, the displacement of the end of the PCB loading arm was calculated assuming negligible deceleration during contact between the massive striker and the PCB arm. The experimentally observed strain-displacement curve up to the maximum load (maximum negative strain) was reproduced reasonably by the CZM. Moreover, the FEM also correctly predicted the failure mode as PC. The sudden drop of the load after the peak in the FEM curve was caused by the rupture of the PCB cracked ligament when it reached the maximum tensile strength (Figs. 6.7b and 6.9c).
This transition, however, occurred smoothly in the experimental curve. The small nonlinearity in the falling slope of the FEM curve was caused by delamination initiation at the PC interface and the abrupt change of the PCB stiffness.

Fig. 6.18. Measured and predicted strain-displacement graph in drop-impact test of Fig. 6.3c.

The sensitivity of the crack initiation location (SC interface or PC interface) to uncertainty in the CZM parameters ($\sigma_{cn}$ or $G_{cn}$) was assessed by performing additional simulations using highly different values of $\sigma_{cn}$ or $G_{cn}$ for both interfaces. An interesting observation was that the crack initiation location was a strong function of the ratio of the PC cohesive strength ($\sigma_{cn}$) to the SC cohesive strength, and independent of the ratio of their fracture energies ($G_{cn}$). Therefore, PC delamination initiation was essentially independent of the toughness ($G_{cn}$) of the PC interface, and was only a function of the relative size of the strength of the relevant PC and SC interfaces. This is consistent with the work of Parmigiani and Thouless [40] who showed that the interfacial crack path in a multi-layered structure is generally determined by its cohesive strength rather than by its fracture energy.
6.5.2. Comparison of fracture loads: experimental data and FEM predictions

Figure 6.19 gives the measured failure loads of the bending specimens as a function of the fillet size, strain rate, and PCB stiffness, and compares them with the FEM predictions. Five specimens of each configuration were tested. The repeatability of the experimental measurements was good, with a standard deviation less than 10% in the measured fracture force. The fracture load increased with the fillet size, strain rate, and PCB stiffness. A stiffer PCB and a larger fillet increased the strength by reducing the stress concentration at the PCB-fillet interface. Moreover, the failure mode changed from SC to PC as the strain rate or fillet size increased. As mentioned before, all specimen fillets were of the as-manufactured size except those made with a large fillet.

It is evident from Fig. 6.19 that the proposed FEM and CZM was able to predict the fracture load of the BGA-PCB specimens with good accuracy; differences were statistically insignificant at the 95% confidence level. In addition, the FEM was able to correctly predict the crack path except for the case of the reinforced PCBs. As mentioned in Section 6.5.1.3 this discrepancy was probably because the multilayer PCB was modeled as a homogeneous orthotropic material, neglecting the stress concentrations resulting from the heterogeneity of its internal structure.

The bending experiments of Fig. 6.3 were repeated using BGA-PCB assemblies underfilled with a relatively stiff silica-filled underfill (Hysol UF3537, 38% silica by volume). This was the same adhesive used in refs. [22, 23]. The FEM described in Section 6.4 with the cohesive parameters of Table 6.3 was used for fracture simulation of these specimens. As in the specimens underfilled with the unfilled underfill (Hysol 3808, properties of Table 6.2), the crack always initiated and grew in the PCB in the specimens reinforced with the filled underfill. Depending on the fillet size, PCB stiffness, and loading rate, the same SC and PC failure modes were observed. The trends of the load-displacement curves were also very similar for filled and unfilled underfills,
and the average fracture loads were identical (t-test, 95% confidence level.). This was consistent with the observation that the underfilled BGA-PCB assembly always failed in the PCB, so that the failure mode and fracture load were independent of the underfill thermal and mechanical properties. This is also consistent with ref. [22], which found that the quasi-static fracture of PCBs was solely a function of underfill fillet size, and was independent of underfill properties.

![Fig. 6.19. Comparison of the experimental average fracture load per unit specimen width (N/mm) with the FEA predictions. Error bars in the experimental data correspond to ±1 standard deviation based on 5 specimens for each case.](image)

**6.6. Conclusions**

Surface and subsurface delamination was observed in multilayer PCBs assembled with microelectronic components (BGAs) and tested under bending loading conditions. A CZM was presented to predict delamination initiation and growth in these BGA-PCB assemblies. For quasi-static loading, the CZM was calibrated using DCB specimens consisting of PCB substrates bonded
with the underfill adhesive. For drop-impact loading, the CZM parameters were obtained from an analytical equation for surface delamination, and a PCB drop-impact test for subsurface delamination. The model was then used to predict transitions in failure mechanisms and the strength of underfilled BGA-PCB specimens fabricated in a surface mount technology (SMT) line, and tested under bending configurations with different loading rates.

The CZM was capable of capturing the overall behaviour of the PCB failure in BGA-PCB specimens and the predicted fracture loads were in reasonable agreement with the measured loads. The model was able to show the increase of the fracture load with PCB stiffness. However, it could not predict the surface delamination failure mode for the reinforced PCB, probably because of the neglect of PCB heterogeneity.

The CZM could also successfully predict the change of the failure mode with underfill fillet size; i.e. surface delamination for relatively small fillets, and subsurface delamination for larger fillets. This was attributed to the lower stress concentration in PCB-fillet interface for larger fillets.

Furthermore, it was found that the crack path was dependent of the strain rate, and changed from surface delamination at quasi-static loading to subsurface delamination with drop-impact loading. An unexpected finding was that the delamination mode predicted in drop impact was only a function of the relative cohesive strengths of the two possible modes (surface and sub-surface), and was independent of the fracture energies of these two possible crack paths.

Overall, it was demonstrated that this model could accurately predict the fracture loads of these underfilled BGA-PCB assemblies. The model was also able to predict the correct crack path as it changed with fillet size and strain rate, but not with increasing PCB stiffness.
6.7. References


Chapter 7

7. Conclusions and future work

7.1. Conclusions

7.1.1. Effect of solder joint length on fracture load and fracture energy

Fracture experiments were conducted on copper-solder-copper double cantilever beam (DCB) specimens to obtain the fracture load and fracture energy as a function of the joint length. An analytical sandwich model as well as a finite element model were used to obtain the variation of the solder peel stresses with the joint length, and correlate it with the fracture load.

- The analytical model showed that the maximum peel stress became independent of the joint length beyond a characteristic length that was a function of the thickness and the mechanical properties of the solder layer and the substrates.

- The solder fracture load increased with joint length before reaching a plateau value of constant joint strength. The characteristic length predicted by the analytical sandwich model was very close to the joint length corresponding to the onset of the joint-strength plateau measured in the DCB specimens.

- The critical strain energy release rate for crack initiation, $G_{ci}$, in the solder layer was independent of the joint length, corroborating its use for fracture load prediction of solder joints. The $G_{ci}$ from a long DCB fracture specimen can be used to predict the fracture of much shorter joints, such as those found in microelectronics surface mount devices.
7.1.2. Effect of joint spacing on load sharing in multiple solder joints

Fracture experiments were conducted to measure the variation of the fracture loads of copper-solder-copper DCB specimens containing two discrete solder joints with the joint spacing. The peel stresses in each joint were predicted analytically using a continuous foundation model. Also, a discrete foundation model was developed to find the resultant peel force in each joint.

- The fracture loads of double joints were maximum at an optimal joint spacing. Finite element analysis (FEA) showed that this corresponded to a minimization of the solder peel stress and an optimal load sharing among the two joints.

- The continuous foundation model predictions of the solder peel stresses were in reasonable agreement with the FEA, although it underestimated the maximum peel stress. The model, however, did not provide an analytical close-form expression for the optimal spacing as a function of solder and substrate properties.

- The discrete foundation model provided a closed-form expression for the resultant peel force acting on the critical solder joint in a double-joint configuration. Its predictions of the effect of joint spacing were in good agreement with those of the FEA for model DCB-type configurations and a microelectronics chip resistor on a printed circuit board loaded in bending. The model isolated the effects of joint geometry and the bending stiffness of the solder and substrates.
7.1.3. Effect of underfill fillet size on the bending strength of underfilled BGA-PCB assemblies

The effect of fillet size on the strength of underfilled ball grid array (BGA) solder joints was characterized using fracture experiments of DCB specimens. Three different underfills with a broad range of thermal and mechanical properties were tested.

- The underfill induced a significant stress concentration in the printed circuit board (PCB) close to the underfill fillet, which resulted in crack initiation in the PCB. In no case, did the underfill layer itself crack.

- For relatively small fillets, or when there was no fillet, cracks initiated and propagated at the interface between the solder mask and the first conducting layer of the PCB. Larger spew fillets decreased the stress concentration at the solder mask-conducting layer interface, and shifted the crack to the interface between glass fibers and epoxy of one of the prepreg layers within the PCB.

- The crack initiation load of the specimens with a large fillet was approximately 100% larger than the case where there was no fillet. FEA showed that the maximum first principal stress in the PCB close to the PCB-fillet interface decreased as the fillet became larger, thereby explaining the measured increase in bending strength with fillet size.
7.1.4. Comparison of underfilling and edge-bonding: effect of processing parameters

The bending strength of underfilled and edge-bonded BGA-PCB assemblies was compared using DCB specimens. Two room-temperature-cure viscous adhesives were used for edge-bonding, while two heat-cure low-viscosity adhesives were selected for underfilling. The effects of fillet curvature, the underfill layer length, and cure temperature on fracture load and failure mode was investigated.

- Regardless of adhesive properties, the bonding method (i.e. underfilling vs. edge-bonding), and processing parameters, all specimens failed in the PCB, and in no case did the adhesive layer itself crack. This was consistent with measurements of the crack initiation strain energy release rate for cohesive failure within the adhesives, which was significantly larger than that of PCB interfacial failure.

- A finite element model showed that the stress distribution in the PCB near the PCB-fillet interface in both edge-bonded and underfilled specimens was only a function of the adhesive fillet size and shape, and independent of the adhesive thermal and mechanical properties, and independent of the extent of the adhesive layer between the BGA and the PCB. For this reason, decreasing the fillet radius of curvature in edge-bonded specimens, reduced stress concentration at PCB-fillet interface, and increased the joint strength.

- The predominant failure mode in the edge-bonded specimens was cracking at the interface between the solder mask and the first conducting layer of the PCB, and in the underfilled specimens was PCB subsurface cracking in a prepreg layer. The different cure temperature of the underfill and edge-bond adhesives was responsible for this difference in failure modes, because it influenced the thermal residual stresses.
7.1.5. Delamination prediction in multilayer PCBs with bonded BGAs

A cohesive zone model (CZM) was developed to predict delamination initiation and propagation in BGA-PCB assemblies under bending. PCB surface and subsurface delamination was observed in BGA-PCB assemblies. For quasi-static loading, the cohesive parameters were obtained from fracture test of DCB specimens consisting of PCB substrates bonded with the underfill adhesive. For drop-impact loading, the cohesive parameters were obtained from a PCB drop-impact test for subsurface delamination, and an analytical equation for surface delamination.

- There was a reasonable agreement between the predicted fracture loads and the measured ones. The CZM correctly predicted the increase of the fracture load with PCB stiffness. But it could not predict the surface delamination failure mode for the reinforced PCB, probably because of the neglect of heterogeneity of the internal structure of the PCB.

- For relatively small fillets the failure mode was surface delamination, while it was subsurface delamination for larger fillets. The model was able to predict this change of the failure mode with underfill fillet size.

- The model successfully predicted the strain rate dependency of the failure model, i.e. surface delamination at quasi-static loading, and subsurface delamination under drop-impact loading. The delamination mode was only a function of the relative cohesive strengths of the two surface and subsurface interfaces, and was independent of the fracture energies of these two possible crack paths.
7.2. Future work

In the bending specimens of the present research, the bending axis was parallel to the first row of solder balls. A more general loading, in which the axis of bending is in the plane of the PCB but not parallel to first row of solder balls would be a useful extension of the present model.

Surface and subsurface delamination was observed in the multilayer PCBs tested in this research. It is of interest to examine the generality of the observations by studying the possible effects on the fracture behavior of changes in PCB layup as well as distribution of copper traces within the PCB conducting layers.

The present finite element model of the PCB neglected the complex 3D structure of the PCB, modeling it as a 2D homogeneous material. The 2D model predictions of the fracture load and failure mode were in reasonable agreement with the experimental data; however, the model could not predict the surface delamination failure mode in the stiffer, reinforced PCBs. A detailed 3D model of the PCB, which takes into account the 3D distribution of copper traces in the PCB and the stress concentrations and interfaces between different PCB constituents (e.g. copper, solder mask, and glass fibers) may better represent the stress state in the PCB due to mechanical loading and hence improve the accuracy of the crack path prediction.

The bending experiment of the edge-bonded BGA-PCB assemblies cured at different elevated temperatures showed that the crack path and failure mode change with cure temperature, indicating the importance of thermal stresses. Thermal stresses, especially those resulting from thermal fatigue, are one of the main reliability issues in solder joints. Therefore, it seems necessary to evaluate the combined effect of thermal cycling and mechanical loading under different strain rates on the fracture behavior of underfilled BGA-PCB assemblies. Thermal fatigue may degrade
the interfacial strength of different interfaces in a component-solder-PCB structure thereby affecting the crack path and the mechanical performance of the assembly under bending loads.