Fatigue fracture of SnAgCu solder joints by microstructural modeling

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Abstract The ongoing miniaturization trend in the microelectronic industry enforces component sizes to approach the micron, or even the nano scale. At these scales, the underlying microstructural sizes and the geometrical dimensions are comparable. The increasing influence of microscopic entities on the overall mechanical properties makes conventional continuum material models more and more questionable. In this study, the thermomechanical reliability of lead-free BGA solder balls is investigated by microstructural modeling. Microstructural input is provided by orientation imaging microscopy (OIM), converted into a finite element framework. Blowholes in BGA solder balls are examined by optical microscopy and a statistical analysis on their size, position and frequency is conducted. Combining the microstructural data with the appropriate material models, three dimensional local models are created. The fatigue life of the package is determined through a critical solder ball. The thermomechanical reliability of the local models are predicted using cohesive zone based fatigue damage models. The simulation results are validated by statistical analyses provided by the industry.

Keywords Lead free · Cohesive zone modeling · Solder fatigue · Microstructural modeling

1 Introduction

In microelectronic packages, mechanical integrity and electrical connection is provided by solder connections. The microelectronics industry has switched to lead-free solders in 2006 due to the toxicity of lead (Pb). Since then, near-eutectic and eutectic compositions of SnAg-Cu alloy are being extensively used as a replacement for the traditional SnPb solder. Solder joints in microelectronic devices are exposed to thermomechanical fatigue loading caused by the repeated heating and cooling of the device (Fig. 1). The different thermal expansion coefficients (CTE) of package materials provoke cyclic mechanical strains which results in fatigue crack initiation and propagation in solder joints. On top of the CTE mismatch, Sn based solders are prone to thermal fatigue deformation even without being mounted on the package. The thermal anisotropy of the β -Sn phase causes intergranular fatigue damage upon cyclic thermal loading (Matin et al. 2006; Subramanian and Lee 2004; Telang et al. 2004; Vianco et al. 2004). Miniaturization, i.e. decreasing solder dimensions, pronounces this kind of failure since smaller joints with a few grains are more likely to fail by intergranular crack propagation.

Examples of both damage mechanisms are shown in Figs. 2 and 3. In Fig. 2, cross-section of a BGA

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Fig. 1 Solder joints are exposed to cyclic thermomechanical loading resulting from the CTE mismatch between package components

package with eutectic SnAgCu solder balls subjected to 2,000 thermal cycles between -40 and 125°C is shown. In Fig. 3, a micrograph of a bulk SnAgCu specimen after 1,500 thermal cycles (between $\Delta T = -40$ and 125°C) is shown. Here it is important to note, the bulk sample was only thermally cycled, it was mechanically not constrained. Thus it is clear that there is an intergranular damage mechanism, although not visible in the presence of a joint. In a solder joint, cyclic thermomechanical loading causes highly localized deformations at the bump/pad interface, generally leading to overall failure. Thermal cycling of bulk specimens clearly reveals that extensive deformation on Sn grain boundaries also takes place. Both damage mechanisms are clearly dependent on the microstructure due to their interfacial nature. Motivated by this fact, cohesive zone based interfacial fatigue damage models were characterized by inverse modeling through dedicated fatigue experiments (Erinc 2007; Erinc et al. 2004, 2005, 2007, 2008). Fatigue life predictions for BGA or flip-chip sol-

Fig. 2 SnAgCu BGA solder balls, $N=2,000$, $\Delta T = -40$ to 125°C , courtesy of J. W. C. de Vries, Philips Applied Technologies, optical image

Fig. 3 Bulk SnAgCu, $N=1,500$, $\Delta T = -40$ to 125°C , optical image

der balls are generally conducted using Coffin-Manson, J-integral, power law or creep models (Tchankov et al. 2008; Desai et al. 1998; Li and Wang 2007; Tee et al. 2003; Zhang et al. 2008). In Towashiraporn et al. (2005), the fatigue crack trajectory and fatigue life of a solder joint is predicted through a coupled numerical-experimental approach. A review on solder joint fatigue models with respect to their applicability to chip-scale packages is given in Lee et al. (2000).

Driven by the ongoing miniaturization trend in the microelectronics industry, the underlying microstructural sizes and the geometrical dimensions of micro-electronic components tend to be comparable. The increasing influence of microscopic entities on the overall mechanical properties makes continuum material

models increasingly questionable. In this study, the thermomechanical reliability of lead-free BGA solder balls is investigated by microstructural modeling. The original aspects which will be presented in this study are: (i) full microstructural model of SnAgCu solder balls based on crystallographic scans, (ii) cohesive zone based damage modeling of physical interfaces that are prone to fatigue damage in solder balls, and (iii) extending the current cohesive zone based microstructural damage framework to three dimensions. It will be shown that microstructural modeling adequately predicts and explains the diversity and scatter in the available experimental data. The SnAgCu solder ball crystallography and manufacturing defects (i.e. voids, under-flow, over-flow etc.) are discussed in Sect. 2. The numerical techniques and the simulations, including the cohesive zone formulation, microstructural modeling and fatigue life predictions are presented in Sect. 3.

2 Experimental analysis

2.1 Blowholes in solder balls

In lead-free solders blowholes or surface voiding is more common than tin lead solders. This is due to the surface tension of the alloy as well as interaction with the flux. Common tin fluxes are ammonium chloride and rosin. Blowholes are caused by outgassing of flux, moisture or other organics on board. Liquids or gases expand at high temperatures escaping through the solder, leaving behind a hole. Water based fluxes that are not completely dried can also expand and cause the same effect (see http://www.circuitnet.com/articles/article_40062.shtml). Small voids may arrest a propagating crack and enhance fatigue life, however, voids that cause a significant decrease in the cross-sectional area of the solder joint may reduce the fatigue life. Determining an optimum void size for crack arrest is out of the scope of this study. Nevertheless, a statistical analysis on the occurrence of voids in BGA solder balls is presented below.

Six newly manufactured EFSOT-BGA256 packages were cross-sectioned row by row. These packages contain 256 solder balls (760 μm diameter) having the composition Sn4.0Ag0.5Cu with Ni/Au metallization. The solder balls were examined by an optical microscope for defects at their maximum diameter ($\sim 760 \mu\text{m}$). The images were collected by a digital camera and void dimensions were measured using image analysis soft-

ware. Figure 4 shows typical examples of manufacturing defects encountered in BGA solder balls. Apart from blowholes, defects due to under-flowing were also observed. The data collected from these packages is plotted in Fig. 5. From all the solder balls examined, 57% contained voids with an average of 1.5 void per ball. 84% of all the voids observed were on the chip side. Solder balls with big voids ($>100 \mu\text{m}$) were rare ($<1\%$). If the solder contained a void, in almost half of the cases the voids volume was less than 1% of the solder ball volume.

Since a single cross-section is used to analyze a solder ball, the results shown in Fig. 5 regarding the presence and the dimensions of the voids are expected to be an underestimate of the three dimensional reality. It is unlikely that the cross-section analyzed corresponds to the largest diameter of a void. Similarly, voids under or above the cross-sectional plane are not taken into account. Three-dimensional imaging techniques, i.e. X-ray tomography, can be used for a more realistic analysis of the void distribution.

2.2 Crystallography

In order to establish a microstructural database for SnAgCu solder balls, 760 μm diameter solder balls were scanned by Orientation Imaging Microscopy (OIM). Solder balls were molded in a 2 mm thick epoxy resin, mixed with copper powder to prevent charging. A standard metallographic surface preparation technique for solder alloys was applied to the cross-section corresponding to the maximum diameter of molded solder balls.

OIM scanning was done using a 30 kV acceleration voltage and a 3 μm step size. The results shown in Fig. 6 are generated such that: (i) minimum neighbor confidence index was set to 0.8 out of 1, (ii) grain dilatation tolerance angle was set to 5° , (iii) a single orientation is present in each grain. Approximately 20% of the solder balls appear to be single crystal, 50% contain 2–5 grains and the remaining 30% contain 6–11 grains. The crystallographic orientation of each grain is given by its Euler angles.

3 Numerical analysis

3.1 Cohesive zone model

Cohesive zones were developed to model crack initiation and propagation as the separation between two

Fig. 4 Manufacturing defects, i.e. blowholes (left), under-flow gaps (right), in BGA packages with SnAgCu solder balls, optical images $\times 20$

Fig. 5 Distribution of blowholes in BGA packages. **a** Volume occupied by void(s) in a solder ball. **b** Void position. **c** Void diameter

surfaces resisted by cohesive tractions (Dugdale 1960; Barenblatt 1962; Needleman 1990). Static cohesive zone models are extended to model interfacial failure under cyclic loading (Foult et al. 1998), whereas other models have been proposed on the basis of the incorporation of a damage parameter (de-Andres et al. 1999). Based on previous formulations (Roe and Siegmund 2003) an irreversible damage model for interface fatigue crack growth is described. In the work of Abdul-Baqi et al. (2005), thermomechanical fatigue damage in solder balls is modeled in 2D by a cohesive zone approach, where a damage evolution law

specific for solder materials is proposed. In the current study, this model is extended to 3D.

Traditionally cohesive zones were used to describe glued surfaces. In the context of a solder joint, the glue layer is analogical to the intermetallic compound layers at the bump/pad interface. A linear traction-separation law is used to define the cohesive traction, depending on the instantaneous stiffness. Stiffness decreases irreversibly as cyclic deformation advances. In this way the fatigue damage history is taken into account. A direction-dependent damage variable is used for the bump/pad interfaces. This is substantiated on previous

Fig. 6 OIM scans of 760 μm diameter Sn4Ag0.5Cu solder balls

experimental evidence (Erinc et al. 2007). At the bump/pad interface, the microstructural morphology (scalops growing perpendicular to the pad surface) causes the anisotropic behavior. The damage law is isotropic for the grain boundaries (Erinc et al. 2008).

The 3D cohesive zone element is shown in Fig. 7. The local coordinate system is defined on the mid-plane ABCD of the element. Four integration points, x_1 , x_2 , x_3 and x_4 in the mid-plane are considered. Two tangent vectors, $\vec{t}_1$ and $\vec{t}_2$, are located on the mid-plane, and the normal vector $\vec{n}$ is found by the cross-product of $\vec{t}_1$ and $\vec{t}_2$. Opening vectors $\vec{\Delta}$ at the integration points are calculated from the global openings at element nodes and rotated to the local coordinate system. Using the components of $\vec{\Delta}$, the components of the traction vectors in the normal and tangential directions, T_n , T_{t1} and T_{t2} , are calculated as follows:

$$T_i = k_i(1 - D_i)\Delta_i, \quad \text{where } i = n, t_1, t_2 \quad (1)$$

where k_i is the initial stiffness and D_i is an interfacial damage variable, which takes values between 0 (no damage) and 1 (complete failure). The slope of the T_i – Δ_i curve for each fatigue cycle gives the instantaneous stiffness, $k_i(1 - D_i)$, which decreases due to the evolution of damage. The damage variable of a cohesive zone element is the sum of the cyclic incremental damage values for that element. The tangential traction components, T_{t1} and T_{t2} , and opening components, Δ_{t1} and Δ_{t2} , compose the total tangential traction, T_t , and the total tangential opening, Δ_t , respectively:

$$T_t = (T_{t1}^2 + T_{t2}^2)^{\frac{1}{2}} \quad (2)$$

$$\Delta_t = (\Delta_{t1}^2 + \Delta_{t2}^2)^{\frac{1}{2}} \quad (3)$$

Fig. 7 3D cohesive zone element, the mid-plane, and local coordinate system

The evolution of the damage variable, $\dot{D}$, is calculated according to:

$$\dot{D}_{n,t} = c_{n,t} |\dot{\Delta}_{n,t}| (1 - D_{n,t} + r)^{m_{n,t}} \left\langle \frac{|T_{n,t}|}{1 - D_{n,t}} - \sigma_f \right\rangle \quad (4)$$

where $\dot{\Delta}_{n,t}$ is the rate of relative opening, σ_f is the fatigue limit, c , m and r are constants controlling the decay of the cohesive interaction. Macaulay brackets ensure that damage evolution takes place only for stress levels higher than the fatigue limit, σ_f . The fatigue limit is taken equal to $\sigma_f = 17$ MPa (Erinc et al. 2007). The evolution law is typically phenomenological and is formulated in a way which takes into account the main damage characteristics in solder joints.

3.2 The local model

A BGA package can be considered as a matrix of geometrically repetitive blocks composed of a solder ball, the printed circuit board PCB, and the molding compound, as shown in (Fig. 8). In this section, detailed damage analyses on such blocks, further denoted as ‘local models’, are presented.

In order to make a local model, first a 3D solder ball incorporating the local grain orientations and the microstructure is created. As explained in Sect. 2.2, 760 μm diameter solder balls were scanned by OIM. Grain boundaries are identified as a collection of line vectors and local Euler angles are used for each of the grains. The grain boundary lines are directly transferred to the finite element discretization, along which the elements are generated. The grain interiors are meshed using hexagonal elements. The crystallographic orientations are assigned to the grains after rotating the local

Fig. 8 A BGA package is a 2D array of geometrically repeating blocks composed of the solder ball, molding compound and the PCB

Euler angles (ϕ , θ , ψ) from the OIM reference frame to the FEM reference frame. The OIM and FE reference frames are shown in Fig. 9.

OIM scans relate the local crystal orientation to the OIM reference frame in Euler angles (ϕ , θ , ψ) according to the x-convention. The local crystal orientation with respect to the OIM reference frame is found by three subsequent rotations; 1st rotation around normal direction (ND) by an angle ϕ (R_ϕ), 2nd rotation around reference direction (RD) by an angle θ (R_θ) and 3rd rotation again around ND, by an angle ψ (R_ψ). The OIM reference frame is rotated to the FE reference frame by R_{OIM} (90°). All four rotations are combined in a single rotation matrix R according to Eq. 5, where each matrix is defined with respect to the vector basis resulting from the previous rotation step.

$$R = R_\phi R_\theta R_\psi R_{\text{OIM}} \quad (5)$$

where

$$R_\phi = \begin{bmatrix} \cos \phi & -\sin \phi & 0 \\ \sin \phi & \cos \phi & 0 \\ 0 & 0 & 1 \end{bmatrix} \quad R_\theta = \begin{bmatrix} 1 & 0 & 0 \\ 0 & \cos \theta & -\sin \theta \\ 0 & \sin \theta & \cos \theta \end{bmatrix}$$

Fig. 9 OIM set-up and OIM and FEM reference frames

$$R_{\psi} = \begin{bmatrix} \cos \psi & -\sin \psi & 0 \\ \sin \psi & \cos \psi & 0 \\ 0 & 0 & 1 \end{bmatrix} \quad R_{\text{OIM}} = \begin{bmatrix} 0 & 1 & 0 \\ -1 & 0 & 0 \\ 0 & 0 & 1 \end{bmatrix}$$

A preliminary 3D analysis is performed on the microstructural meshes, in order to coarsen the discretization without substantially altering the overall mechanical response. The discretization is coarsened on a scale of $f=0-10$, where $f=0$ corresponds the mesh created according to the original scan. An elasto-plastic material model is used (Erinc et al. 2007). The models are thermally loaded by linearly increasing the temperature from 25 to 100 °C homogeneously in 30 min, in other words, temperature is prescribed to all elements as a state variable and a heat transfer calculation is not done. The distribution of the equivalent Von Mises stresses, σ_{vm} , at 100 °C is compared. Selected meshes and contour plots of σ_{vm} are shown in Figs. 10 and 11, respectively. After coarsening four levels, $f=4$, the number of elements significantly reduces. Coarsening level $f=6$ seems to be an adequate compromise since at this level the details in the results are well captured while the number of elements is reduced by a factor of two.

The microstructure of ten solder balls is discretized from the OIM scans incorporating local crystallographic orientations and coarsening the mesh by a factor $f=6$, as explained above. Different from the preliminary analysis shown above, very thin cohesive zone elements are now placed at the grain boundaries, such that the normal direction corresponds to the crack opening between two grains, while the two tangential directions reside in the grain boundary plane, representing grain boundary sliding. Two examples of microstructural meshes and the cohesive network at the grain boundaries are shown in Fig. 12.

The grain boundaries in the thickness direction are assumed to be straight and perpendicular to the surface. The resulting grain orientations are thereby approximately expressed in the FEM coordinate system. In

Erinc (2007), it is shown on the basis of numerical results that vertical grain boundaries touching the bump/pad interface have the highest stress level under thermal loading, whereas the stress level at a grain boundary decreases as it becomes more horizontal.

Next, solder balls having a contact angle of 120° are extruded from the discs, which are then combined with the rest of the local model shown in Fig. 13. The local model consists of (top to bottom): molding compound, substrate, solder mask, metallization (UBM: copper, nickel and Ni₃Sn₄ intermetallic layer), solder ball, metallization, solder mask and the printed circuit board. The elastic and thermal material properties are given in Table 1. SnAgCu is modeled by a time independent elasto-plastic material model (Erinc et al. 2007) combined with a steady-state creep model for bulk SnAgCu given by Wiese et al. Wiese and Wolter (2004), shown in Eq. 6 where A_1 and A_2 are $4e-7$ and $1e-12 \text{ s}^{-1}$, Q_1 and Q_2 are 26.8 and 61.4 kJ/mol, n_1 and n_2 are 3 and 10 (different from the original value), respectively. The plasticity model used is based on Von Mises yield criterion. Hardening is described by providing the measured tensile curve using proper stress and strain definitions. Besides the grain boundary elements, two sets of cohesive zone elements are placed between the metallization and the solder ball on both sides. The damage evolution parameters for the bump/pad and grain boundary elements have been identified previously (Erinc et al. 2007, 2008) and are tabulated in Table 2.

$$\dot{\epsilon} = A_1 \left(\frac{\sigma}{\sigma_N} \right)^{n_1} \exp \left(-\frac{Q_1}{RT} \right) + A_2 \left(\frac{\sigma}{\sigma_N} \right)^{n_2} \exp \left(-\frac{Q_2}{RT} \right) \quad (6)$$

Since all physical interfaces have a very small, though finite thickness, cz elements are given a 100 nm initial thickness (t_{cz}). In classical cohesive zone approaches, cohesive zones do not need to have an initial thickness. However, assigning a physically relevant

Fig. 10 Reducing the number of elements (N) by coarsening the mesh by a factor f. **a** $f = 0$, $N = 564$. **b** $f = 5$, $N = 360$. **c** $f = 10$, $N = 140$

Fig. 11 Corresponding equivalent Von Mises stress [MPa] distribution at 100 °C

finite thickness allows one to calculate a finite initial stiffness which prevents the known initial stiffness problem and better represents the actual interphase (Chaboche et al. 2001). The initial stiffness of the cohesive elements for normal, k_n , and tangential, k_t , directions are computed from the adjacent materials 1 and 2, in order to provide an equivalent elastic deformation compared to the case without the cohesive elements. The initial stiffness for tangential and normal directions are given in Eq. 7, where E is the Young's modulus and G is the shear modulus.

$$k_t = \frac{2G_1G_2}{t_{cz}(G_1 + G_2)}, \quad k_n = \frac{2E_1E_2}{t_{cz}(E_1 + E_2)} \quad (7)$$

Periodic boundary conditions are applied to the local model. A cyclic thermal loading is applied, being either cyclic harmonic $\Delta T = -40$ to 125°C with a moderate temperature change rate (Fig. 14a), or cyclic shock $\Delta T = -55$ to 125°C with a strong temperature change

rate (Fig. 14b). Each cycle is computed in 10 increments.

3.3 Fatigue life predictions

In the literature (Abdul-Baqi et al. 2005), D_n and D_t are averaged to calculate an effective damage value D_{eff} according to the following formulation:

$$D_{\text{eff}} = (D_t^2 + D_n^2 - D_t D_n)^{\frac{1}{2}} \quad (8)$$

According to Eq. 8, a D_{eff} value lower than either D_n or D_t can be calculated, leading to a decrease in the value of D_{eff} (see Fig. 15, left). This is physically unrealistic since damage is irreversible and cumulative. In this study a modified formulation is proposed, given in Eq. 9:

$$D_{\text{eff}} = \frac{2}{\pi} \arccos \left(\cos \left(\frac{\pi}{2} D_n \right) * \cos \left(\frac{\pi}{2} D_t \right) \right) \quad (9)$$

Fig. 12 Microstructural meshes and the cohesive network at grain boundaries.
a Example 1, **b** Example 2

Fig. 13 A representative local model

D_{eff} is defined in the range [0–1]. For low values of D_n and D_t , D_{eff} follows a circular pattern (see Fig. 15, right). As they approach 1, D_{eff} tends to a square pattern. D_{eff} is thereby never smaller than its composing values D_n or D_t . The term $2/\pi$ is added to normalize

D_{eff} towards unity. Values of D_n and D_t are calculated at the end of every increment for every cz element according to Eq. 4.

After D_{eff} is calculated for all elements, an average damage value per interface is computed taking

Table 1 Elastic material parameters and CTE values used in simulations

Material	E(GPa)	ν	α (ppm/°C)
SnAgCu ^a	64.1	0.4	[100], [110]:16.5, [001]:32.4
Cu	128	0.35	17
Ni	197	0.31	12.96
Ni ₃ Sn ₄ ^b	140.4	0.3	14.98
PCB ^c	17.5	0.11	17.6
Moulding compound ^c	10	0.25	16.9
Substrate ^c	1	0.11	16
Solder mask ^c	2.412	0.467	60

^a Hardening data is given in[Erinc et al. \(2007\)](#)^b [Jang et al. \(2004\)](#)^c [Lai et al. \(2005\)](#)**Table 2** Cohesive zone element parameters used in simulations

Cz elements	k(GPa/mm)	c	m	r
bump/pad, normal ^a	8.79e8	68,000	3.16	1e−6
bump/pad, tang. ^a	3.21e8	47,000	3.135	4e−5
grain bound, normal ^b	6.4e8	42,000	2.94	0
grain bound, tang. ^b	2.28e8	42,000	2.94	0

^a [Erinc et al. \(2007\)](#)^b [Erinc et al. \(2008\)](#)**Fig. 14** Cyclic harmonic (left) and cyclic shock (right) loading schemes prescribed as boundary conditions in the simulations

the surface average. The surface average is calculated by weighing the integration point values to the corresponding face area of the element. Figure 16 shows the average effective damage $D_{\text{eff}}^{\text{avg}}$ at the interfaces, (1) top bump/pad interface, (2) bottom bump/pad interface, (3) grain boundaries. In all computations, the top bump/pad interface shows the highest damage, followed closely by the bottom bump/pad interface. Least damage was observed at the grain boundaries. Damage at the grain boundaries always evolved to a stationary value early in the calculation. If the solder geometry is chosen such that it concentrates more stresses in the solder, i.e. through an hourglass shape, the grain boundaries will take most of the damage and eventually fail, whereas damage at the pads level-off. Hence, there is a competition between the grain boundaries and the pads favored by a specific geometry.

Substantial CPU time can be saved if damage evolution could be predicted at an earlier stage in the calculation. Local models (examples 1 and 2 in Fig. 12) are loaded under thermal cycling, $\Delta T = -40$ to 125°C , for $N = 5,000$ cycles. In Fig. 17, the average damage evolution versus the number of cycles for both models is plotted. In connection with the damage evolution law given in Eq. 4, the shape of the curve suggests an exponential evolution ($1 - e^{-a*N}$). However, a single exponential parameter is not able to describe the entire span of the damage evolution curve. Instead, a rational function of quadratic polynomials with five parameters $(p_1x^2 + p_2x + p_3)/(x^2 + q_1x + q_2)$ is used. As obvious from Fig. 17, with five parameters, the damage evolution curve can be described and predicted fairly well. The fitting function is determined over the simulation results between $N = 0$ and $N = 1,000$. Beyond

Fig. 15 Contour plot of D_{eff} according to: Eq. 8 (left), Eq. 9 (right)

Fig. 16 Damage at all interfaces in a representative local model with cyclic shock loading

Fig. 17 Damage evolution curve is fit by a quadratic rational function (rat22) between $N=0$ and $N=1,000$ cycles and extrapolated to $N=5,000$ cycles. Extrapolation is compared with numerical results

$N=1,000$, the extrapolated curve is compared with the rest of the simulation results using least squares minimization, which provides an adequate prediction up to $N=5,000$.

3.4 Numerical-experimental comparison

The numerical results obtained so far are compared with experimental analyses of BGA packages under cyclic harmonic and cyclic shock loading. For both types of loading, damage evolution in all local models is calculated until $N=1,000$ cycles and extrapolated to $N=5,000$ cycles. According to Eq. 9, a critical effective damage value D_{eff} is defined to predict the number of cycles to failure, that adequately reproduces the experimental failure distributions provided by Philips Applied Technologies Eindhoven.

First, the fatigue life of all local models are calculated without any defects, representing a theoretical case. Next, defects are introduced in the mesh, as statistically determined in Sect. 2. This is done by setting the initial damage value of the cz elements to $D=1$ at places where a void is expected. In the case with defects, the average number of voids in a solder balls was 1.5, 80% of the voids contained a void with diameter $\phi \leq 50 \mu\text{m}$ (74% measured), 20% of the voids contained a void with diameter $\phi \leq 100 \mu\text{m}$, (25% measured). Voids with a diameter larger than $100 \mu\text{m}$ are not modeled (1% measured). Eighty percent of the voids were placed at the chip side (84% measured). Finally, a data set consisting of good (45%) and defective balls (55%) is constructed. Figure 18 shows that, with a critical damage level for failure equal to $D_{\text{eff}}^{\text{crit}} = 0.87$, a good agreement between the statistically constructed data set and the experimental data is obtained.

Fig. 18 Comparison of experimental results (De Vries et al. 2007) with simulations for harmonic cycling (top), and cyclic shock (bottom)

The fatigue life of strongly resistant ($N > 5,000$) solder balls is over-estimated. This is an expected result; as mentioned previously, the cross-sectional defect analysis hides many defects, yielding too optimistic results (45% of all solder balls were defect-free). In reality, more defects may be present, which have to be inserted into the local models.

4 Conclusions

In this study, the thermomechanical reliability of lead-free BGA solder balls is investigated by microstructural modeling. Three dimensional solder joints are simulated incorporating the microstructure, local orientations and initial defects, which were not handled in the literature before. The fatigue life is determined us-

ing cohesive zone based damage models for bump/pad crack propagation and intergranular fatigue damage and compared with experimental values. The following conclusions are drawn from the current study:

- Microstructural modeling allows one to predict and understand the scatter in the solder ball fatigue life observed in the engineering practice.
- A 2D cross-sectional analyses gives a qualitative indication on the present initial defects. However, the numerical-experimental comparisons show that a 2D methodology yields an underestimate of initial defects in real solder balls. 3D visual techniques have to be used for a complete description of the geometry.
- Using cohesive zone elements to describe interfacial fatigue damage in solder joints, which is extended to 3D in this paper, is shown to be an effective tool in fatigue life prediction.
- The numerical results are compared with experimental fatigue life analyses obtained from the industry. A critical effective damage value, describing the onset of failure for cohesive zone elements is determined based on the numerical-experimental comparison. With the incorporation of initial defects into the models, an adequate agreement between the predicted and measured fatigue lives of solder joints is achieved.

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