A STUDY OF FACTORS AFFECTING SOLDER JOINT FATIGUE LIFE OF THERMALLY ENHANCED BALL GRID ARRAY ASSEMBLIES

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ABSTRACT

In this study, a finite element study of design factors, including material constants and geometry parameters, affecting fatigue life of solder joints of a typical cavity-down Thermally Enhanced Ball Grid Array (TEBGA) assembly subjected to an accelerated temperature cycling load is performed. In order to precisely characterize the fatigue life of solder joints, the geometry profile of solder joints within the package is substantially predicted using an energy-based method-the Surface Evolver (see, e.g., Brakke, 1994; Chiang \textit{et al}., 2000). In addition, a finite-volume-weighted averaging technique is proposed to describe the strain/stress response of solder joints at material/geometry discontinuities. The analysis employs a plane strain FE model with the purpose of reducing the computational cost. To validate the FE modeling so as to corroborate the nonlinear and complex mechanical behaviors of materials in the assembly, a typical interferometric displacement measurement method-- Moire interferometry (see, Han and Guo, 1995) and 3-D solid modeling are used. Through the parametric design of the fatigue life of solder joints, the reliability characteristics of the TEBGA assembly are then effectively identified.

I. INTRODUCTION

A cost-effective package that provides great thermal performance is in demand today, due to high power handling applications. Thermally enhanced BGA packages (TEBGA or the so-called Super\textsuperscript{R} BGA, see Figs. 1 and 2) provide useful and relatively cost-effective solutions to the poor thermal performance
problems that exist in conventional over molded plastic BGA packages. In contrast to a conventional face-up chip design, such as PBGA packages, TEBGA packages adopt a face-down chip configuration, in which a copper plate is directly attached to the back of a chip and to an organic substrate in order to increase cooling rate.

Similar to conventional face-up chip design of PBGA assemblies, the solder joints within TEBGA assemblies not only perform as a thermal passage system for conducting the heat that is generated from the chip but also as a mechanical mechanism for resisting thermal deformation due to the CTE mismatch between packages and PCBs. With solder joint reliability of great concern today, many studies on PBGA reliability problems (see, for examples, Paydar et al., 1994; Hong, 1997; Lau and Pao, 1997) have been reported in the literature. Among these studies, only a few (see, e.g. Pao et al., 1998; Lee and Lau, 1998) carry out reliability analysis of cavity-down TEBGA packages. These reports focus on prediction of the fatigue life of solder joints. It should be noted that reliability of solder joints is a function of many variables, for example, material constants and geometry parameters. It can be expected that through appropriate combinations of these parameters and constants, solder joint reliability of packages can be effectively enhanced. For this particular purpose, parametric study using FE analysis can be very useful and effective. The purpose of this study is to provide designers a more clear, comprehensive insight to the reliability characteristics associated with the chosen design parameters and a reliability-enhanced design guideline at the initial product development stage. Thus, in this study, parametric design of the reliability of the TEBGA assemblies is conducted. It should be noted that the fatigue life of solder joints depends on stress/strain behaviors under thermal deformation, and more importantly, these thermal-mechanical responses are also extensively governed by the geometry profile of the solder joint after solder paste reflow. In order to accurately derive these stress/strain responses, the geometry profile of the solder joint is substantially predicted by using the Surface Evolver (see, e.g., Brakke, 1994; Chiang et al., 2000) based on an energy formulation.

In nonlinear FE analysis, a significant stress/strain concentration field that is highly mesh-sensitive may be generated, particularly at those locations around the corners of solder joints due to significant geometry/material changes. Moreover, the fatigue life of the solder ball as well as overall package reliability are extensively determined by the maximum effective inelastic strain range. It is, therefore, essential to effectively characterize the strain concentration. In the literature, the problem was resolved by using an explicit geometry representation, a fillet, or material-nonlinear modeling. Akay et al. (1997) introduced a volume-weighted averaging technique to characterize the stress/strain response at the geometry/material singular point. However, employment of this approach may unfortunately underestimate the stress/strain response due to the fact that it is volume-weightedly averaged within the entire domain resulting in over rating the fatigue life. In this study, a finite-volume-weighted averaging technique is proposed. Instead of averaging the response within the entire domain, the improved approach averages the structural response in a finite zone that is characterized using an empirical engineering criterion.

Using numerical modeling in order to reduce cost and cycle time has become an integral part of electronic packaging product design and development today. However, due to the nonlinear and complex mechanical behaviors of materials, the accuracy of results derived from modeling becomes vague. If computational modeling can be further verified experimentally, the difficulties can be relieved to some extent, and more importantly, the subsequent predictions associated with variations of the model, such as parametric studies that will be covered in the subsequent section, can be done at a more reliable level. In this study, a typical interferometric displacement measurement method-- Moire interferometry (see, Han and Guo, 1995) and 3-D solid modeling are used to validate the previously proposed FE modeling.
Based on the previously verified FE model together with the Coffin-Manson relationship and the proposed finite-volume-weighted averaging technique, design factors that affect the fatigue life of solder joints of the TEBGA assembly are extensively studied in this paper.

II. A TYPICAL TEBGA PACKAGE AND MODELING

The considered package, as shown in Fig. 1, is a 256-pin, cavity-down TEBGA. It is mounted to a multilayer Printed Circuit Board (PCB) via four rows of perimeter BGA eutectic solder joints (i.e., 63Sn/37Pb) of 1.27 mm pitch. A center cross section of the package is shown in Fig. 1 and 2. The eutectic solder joints are identified with solder-mask-defined (SMD) copper pads on both the package and board. The geometry of the baseline package is square with package size of 27×27 mm. The chip size of the package is 6×6 mm with a thickness of 0.25 mm, and is directly attached to a 0.28 mm thick copper plate by a silver-filled die attach adhesive with a thickness of 0.047 mm. In addition, the package is laminated with a solder mask 0.047 mm in thickness. The size of the PCB that is made of FR-4 material is 32.4×32.4 mm with a thickness of 1.0 mm.

Accordingly, the elastic material properties of these components are listed in Table 1 based on Pao et al. (1998). It should be noted that all the materials in the package are assumed to be linearly elastic except the eutectic solder joint. The eutectic solder is considered as a viscoplastic and temperature-dependent material because creep may occur in the solder joint even at room temperature. In addition, it has been extensively reported that temperature-dependent and time-dependent inelastic strains are the major factors causing solder joint failures. Hence, a non-linear FE model that incorporates temperature-dependent plasticity and time-dependent creep behaviors is constructed to characterize the inelastic stress/strain response of the solder joint. In this study, the Garafalo hyperbolic sine law is applied to model the creep behavior of the solder joint, in which it is given as follows:

\[ \varepsilon_{cp} = \chi \left( \sinh \beta \sigma \right)^\Gamma \exp \left( -\frac{\Delta H}{RT} \right) \]

where \( \varepsilon_{cp} \) denotes the uniaxial equivalent creep strain rate, \( \chi \) is equal to 147.9 (1/sec), \( \beta \) is 0.0805 (1/MPa), \( \Gamma \) is a stress exponent equal to 3, \( \sigma \) is the equivalent stress, \( \Delta H \) is the activation energy equal to 52961 (J/mol), \( R \) is the gas constant that is 8.31 (J/mol/K), and \( T \) is the absolute temperature. Note that these material constants are from Nagaraj and Mahalingam (1993). In addition, the Prandtl-Reuss formulation is employed to simulate the rate-independent plastic deformation. Consider that a typical eutectic solder joint holds a tensile stress-strain relationship, as shown in Fig. 3.

III. FATIGUE LIFE PREDICTION

The fatigue life of the eutectic solder joint can be predicted using an empirical Coffin-Manson relationship (see, Coffin and Schenectady, 1954; Manson, 1965):

\[ N_f = \frac{1}{2} \left( \frac{\varepsilon_{IE}}{\varepsilon_f} \right)^{\frac{1}{c}} \]

where \( N_f \) is the mean cycle to failure, \( \varepsilon_{IE} \) is the inelastic strain range in one cycle of thermal loading, \( \varepsilon_f \) is the fatigue ductility coefficient that is 0.325, and \( c \) is the fatigue ductility exponent. The exponent is generally between -0.5 and -0.7, and is assumed to be -0.5 in this study. Once the cyclic shear strain range of the solder is derived, the corresponding fatigue life can then be estimated.

In order to explore the fatigue life of the
assembly, the analysis model consists of all major segments, including one copper plate for heat dissipation, a chip, a BT substrate, a copper inner ring, solder joints, a die attach, a solder mask, and a PCB. The geometry profile of the solder joint is determined by using the Surface Evolver. Generally, the total energy of a liquid body consists of three major energy portions: the surface tension energy, the gravitational energy, and the external energy that is related to the solder volume change. Based on these energies, the variational energy and restoring force (Eq. (4)) along the gravitational direction of the solder ball can be expressed as:

\[
\delta E = \int_{\Omega} (\nabla \cdot \mathbf{h} - \mathbf{n} \cdot D \mathbf{g} \cdot \mathbf{n}) dA + \rho g \int_{\Omega} (\nabla \cdot \frac{z^2}{2} \mathbf{k} \cdot \mathbf{h}) dA
\]

\[- \nabla \times (\mathbf{h} \times \frac{z^2}{2} \mathbf{k}) \cdot \mathbf{dA} = \int_{\Omega} \mathbf{h} \cdot \mathbf{dA} \] (3)

\[
F_e = \frac{\partial E}{\partial H} = \frac{\partial E_{\text{surface tension}}}{\partial H} + \frac{\partial E_{\text{gravity}}}{\partial H} + \frac{\partial E_{\text{external force}}}{\partial H} \] (4)

where \( E \) is the total energy associated with the solder standoff height \( H \), which includes the surface tension energy on the solder surface, the gravitational energy of the solder, and the energy due to the external forces. Each part of the energy in Eq. (3) can be written as:

\[
\frac{\partial E_{\text{surface tension}}}{\partial H} = \zeta \int_{\Omega} (\nabla \cdot \mathbf{h} - \mathbf{n} \cdot D \mathbf{g} \cdot \mathbf{n}) dA \] (5)

\[
\frac{\partial E_{\text{gravity}}}{\partial H} = \rho g \int_{\Omega} (\nabla \cdot \frac{z^2}{2} \mathbf{k} \cdot \mathbf{h} - \nabla \times (\mathbf{h} \times \frac{z^2}{2} \mathbf{k})) dA \] (6)

\[
\frac{\partial E_{\text{external force}}}{\partial H} = -p \frac{\partial \mathbf{V}}{\partial H} = -p \int_{\Omega} \mathbf{h} \cdot \mathbf{dA} \] (7)

In the above equations, \( \mathbf{h} = [(z_{\text{top}} - z)/(z_{\text{top}} - z_{\text{base}} - H)] \) \( \mathbf{k} \) is a variational vector field, \( \zeta \) is the surface tension, \( \rho \) is the density, and \( g \) is the acceleration of gravity. One way to effectively determine the restoring force of the solder ball along the gravitational direction is to give a downward or upward offset (perturbation) on the solder pad. Different offsets on the solder pad will correspond to different molten ball geometrical shapes and different gravitational restoring forces while maintaining the same solder volume and pad size. The weight of the package is 4.2 grams; thus, the loading for each ball is 16.1 dynes. The volume of each solder joint is 0.2680 mm³, the solder pad radius is 0.287 mm, and the surface tension coefficient is 48.1 dynes/mm. By applying the Surface Evolver, the standoff height of the solder joint is obtained as 0.5436 mm and the ball shape that is projected on the x-y plane is shown in Fig. 4.

**IV. FINITE-VOLUME-WEIGHTED AVERAGING TECHNIQUE**

The stress/strain information near the interfaces of the solder joint and the solder mask and that of the solder joint and the PCB is effectively characterized. Note that the interfaces of these components usually involve an abrupt geometry change that inevitably forms a sharp corner and more importantly, induces a singular (concentrated) stress/strain response. Since the material nonlinearity is considered such that the stress concentration can be eventually eased, techniques that can be applied to characterization of the strain response around the singular point are in critical demand.

In this study, an improved volume-weighted averaging technique is presented to effectively characterize the strain response in the corner of the solder joint. Instead of averaging the state variables over the entire material domain as in Akay et al. (1997), a specific zone is introduced and applied to perform the averaging technique as follows:

\[
\bar{\varepsilon} = \sum_{e=1}^{n_0} \int_{\Omega_e} \varepsilon_e d\Omega / \sum_{e=1}^{n_0} \int_{\Omega_e} d\Omega \] (8)

where \( n_0 \) is the total number of elements in the zone, \( \varepsilon_e \) the strain of the \( e \)-th element, \( \Omega_e \) the volume (area) of the \( e \)-th element, and \( \bar{\varepsilon} \) the volume-weighted
averaging strain in a specific zone. The dimension of the finite zone is determined in an empirical manner. It should be small enough to capture the maximal strain field; on the other hand, large enough to obtain a converging solution as the mesh density increases. To explore the dimension of the particular finite zone, four different FE models, each with a different mesh density in the fan-shape circular sector (i.e., radius is equal to 0.12 mm, as shown in Fig. 4) at the corners of each ball, are applied for modeling the solder joint. The total number of elements in one fan-shape circular sector are 18, 66, 192, and 504, respective to these four FE models. In addition, four different radii, starting from the center of the fan-shape circular sector, are defined, in which they are 0.01, 0.02, 0.04, 0.06 mm respectively. For simplicity, the creep behavior is not considered in the analysis, and in addition, an isothermal net temperature swing of 100°C is used as the thermal loading. Fig. 5 presents the volume-weighted averaging equivalent plastic strain associated with these four different zones.

It can be easily seen from Fig. 5 that, empirically, the 20-micro-meter zone seems to provide more agreement with the currently proposed criterion for determining the dimension of the finite zone than the others. In addition, the curve of the 20-micro-meter zone shows that the average equivalent plastic strain is well converged as the mesh density is up to about 200. This implies that the strain response in the geometry singularity area can be substantially characterized and more importantly, would no more be mesh-sensitive when the mesh is adequately dense. Thus, the characterized 20-micro-meter zone with mesh density model of 192 (i.e., Model-C), as shown in Fig. 5, is applied in FE modeling. In this study, the FE model utilizing a plane strain approximation is included in the parametric analysis. Due to the symmetry of the package, only one half of the package is modeled in the FE analysis. The FE model as shown in Fig. 6 consists of 20973 plane strain elements and 21466 nodes. The symmetry boundary condition is imposed on the symmetry plane of the package and the y-dir displacement in the bottom of the symmetry plane (i.e., Point “A2” in Fig. 6) is constrained in order to prevent it from rigid body motion during the FE analysis. Special attention should be paid to modeling of the die attach component due to its very bad geometry aspect ratio. Maintaining a 4:1 aspect ratio with mapped meshing would not be practical, since a very large model would be yielded. Fortunately, it is observed that the stress/strain through the thickness of the die attach is fairly
uniform such that elements’ aspect ratio in these areas could be more flexible. In this study, 2 elements are applied through thickness while a sufficiently fine mesh density is applied through the x direction.

V. VALIDATION OF THE FEM MODEL

As mentioned previously, the accuracy of the FE analysis results strongly relies on a precise modeling of the nonlinear and complex mechanical behaviors of the materials. In order to precisely validate the previously proposed FE modeling so as to accurately predict the fatigue life of the solder joints, in this study, a typical interferometric displacement measurement method—Moire interferometry was used. Moire interferometry is an optical method that can provide contour information on in-plane displacement fields with the basic sensitivity 0.417 µm/fringe under a virtual reference grating with a high frequency of 2400 lines/mm. As the package size continues decreasing, the sensitivity of available interferometers on the market will not be sufficient. For the purpose of improving displacement sensitivity, particularly for smaller electronic packages, the interferometric displacement measurement methods usually incorporate a phase-shifting technique. With the fringe shifting technique, the sensitivity can be enhanced up to one hundredth of the fringe order.

Moire interferometry has been extensively applied for thermal deformation analyses in electronic packaging product development (Han and Guo, 1995). Its principle can be described in brief as follows. A crossed-line diffraction grating is first replicated on the surface of the specimen. This high frequency grating deforms together with the underling specimen. A virtual grating is generated in the intersection zone by two coherent light beams. Furthermore, a reference grating that interrelates with the second set of lines in the crossed-line specimen grating is produced by using similar light beams in the vertical plane. These two gratings result in fringe patterns, representing contours of constant $U$ ($x$-dir) and $V$ ($z$-dir) displacement field. Based on the fringe patterns, the displacement can be determined from the fringe orders as:

$$U = \frac{1}{f} N_x, \quad V = \frac{1}{f} N_z$$  (9)

where $N_x$ and $N_z$ are fringe orders in the $U$ and $V$ field pattern, and $f$ is the frequency of the virtual grating. The strains can then be calculated from the above measured displacement fields as follows:

$$\varepsilon_x = \frac{\partial U}{\partial x} = \frac{1}{f} \frac{\partial N_x}{\partial x}, \quad \varepsilon_z = \frac{\partial V}{\partial z} = \frac{1}{f} \frac{\partial N_z}{\partial z}$$

(10)

The Portable Engineering Moire Interferometer (PEMI) manufactured by Photomechanics Inc. was used to accomplish the measurement of the deformation of the TEBGA package. The experiment conducted for validation of the modeling is as follows. The package is first heated up to 125°C, and then, a crossed-line diffraction grating is replicated on the surface of the central cross section of the package. As soon as the package is cooled down to room temperature (i.e., 25°C), PEMI together with the phase-shifting technique is then applied to capture the orders of the fringe pattern, and then, the displacement field as well as the strain field can be extracted based on Eqs. (9) and (10).
especially in the U field. The maximum difference can be up to 25%. This difference might be due to the essential characteristics of the 2-D plane strain FE approximation. For further verification of the presumption, a 3-D solid FE model of the package is also under construction, and due to symmetry, only 1/8 package is included in the analysis model. The FE model consists of 34419 elements and 40917 nodes. The boundary condition of this model is similar to the 2-D plane strain model, in which the symmetry boundary condition is defined on the symmetry planes, and the node at the bottom of the intersecting line of these two symmetry planes is constrained to move in the z direction. The final FE model is shown in Fig. 8, and the corresponding analysis results are also shown in Table 2. From Table 2, one can see that a close agreement between the results of the measurement and simulation is achieved. This has corroborated the previous deduction, and more importantly, validated indirectly the mechanical behaviors of materials applied in the model.

The CPU time for analysis corresponding to these two FE approximations is also listed in Table 3. It can be observed that the computational effort, using the 3-D FE approximation, is almost 40 times larger than that using the 2-D FE model. For this simple temperature-rise exercise, the computation costs of these two modeling techniques are surprisingly divergent. Temperature cycling exercises would be more expensive. Even though the plane strain FE approximation is not as accurate as the FE 3-D solid approximation, considering the computational cost, the 2-D FE approximation can be applied for the parametric study of the package’s fatigue life. The reported design trend would be still of significance as long as the analysis model is extensively verified and the corresponding failure mechanisms would not be altered. As a matter of fact, the trends of the calculated mechanical behaviors of the package based on these two approaches are identical, indicating that the corresponding failure mechanisms would unlikely be different. On the other hand, if the objective of the study were to predict the real fatigue life of the assembly, the 3-D solid modeling would be the preferred choice.

### VI. PARAMETRIC DESIGN OF RELIABILITY

The design parameters considered in this parametric study can be categorized into two main classes: the geometry parameters and the material properties of components within the assemblies. Since the stress/strain response of packages under thermal deformation depends extensively on the configuration of structures, it is, consequently, important to investigate the dependence of the package’s configuration on the fatigue life. Moreover, considerable differences in the epoxy-based material properties, such as FR-4, may be perceived between vendors. Reports, see, for example, Yeh et al. (1996), have indicated that CTE and Young’s modulus are two of the most pronounced material properties influencing the reliability of packages. Thus, the effect of these material parameters on a package’s reliability is also investigated.

The geometry design parameters comprise the thickness of die/die attach adhesive/PCB/heat spreader, and the size of the die; on the other hand, the material design parameters include the CTE and Young’s modulus of FR-4. The numerically accelerated temperature cycling test condition is composed of 5-minute linear temperature loading/unloading ramp and 20-minute low/high temperature dwell periods, and the test temperature range is from −40 to 125°C. The stress free condition is assumed to be room temperature (25°C). Note that the commercial FE code MARC® is responsible for all the nonlinear
FE analyses conducted in the study.

When a structural system is subjected to a reversal, cyclic stress, and creep strain can be accumulated, resulting in cyclic creep or ratcheting behaviors. Before going further to perform the parametric study, the number of temperature cycles that can stabilize the value of strain range shall be determined *apriori*. The ratcheting maximum shear strain response (i.e., \(\varepsilon_{12}\)) versus the shear stress (i.e., \(\sigma_{12}\)) in a selected element of the Pb37-Sn63 solder joint is shown in Fig. 9. It shows that the stress-stain hysteresis loops will move from right to left. This will create a relative motion between the pad and the Pb37-Sn63 solder paste, and eventually, lead to the initiation of cracking. From Fig. 9, one can also see the following points. First of all, the ratcheting behavior is at a decreasing rate. After the tenth temperature cycle, the cyclic creep continues at a constant stress throughout the analysis period, and more importantly, the strain response reaches a saturated state. Furthermore, the convergence of the strain increment, including the plastic strain increment, the creep strain increment, and the inelastic strain increment that is the sum of the plastic and creep strain increments, versus the temperature cycles is shown in Fig. 10. Note that the total average equivalent inelastic strain increment \(d\varepsilon_{eq}^{in}\) that is used to estimate the thermal cycling creep-fatigue life cycles to failure is defined as follows:

\[
\varepsilon_{eq}^{in} = \sqrt{\frac{2}{3} d\varepsilon_{12}^{in} d\varepsilon_{12}^{in}}
\]  

(11)

in which \(d\varepsilon_{12}^{in}\) denotes the average inelastic strain increment. From Fig. 10, it can be seen that \(d\varepsilon_{eq}^{in}\) (the solid triangle” ▲ “ curve) attains convergence after about 10 thermal cycles. In addition, as the temperature cycle proceeds, the magnitude of the plastic strain increment decreases while that of the creep strain increment increases. Based on the previous observation, the maximum average equivalent inelastic strain increment derived at the eleventh temperature cycle would be employed for evaluating the fatigue life of the most critical solder joint for each parametric analysis.

The results of the parametric reliability design of the TEBGA assembly are shown in the following, in which some parameters will be extensively compared with those derived from the conventional cavity-up PBGA (Wu and Yeh, 1997).

1. Effect of Die Thickness

There are a total of seven different packages in this study, each with a different die thickness, for investigating the effect of die thickness on the solder joint reliability. The thickness of the die ranges from 0.15 mm to 0.55 mm while all the other material property constants and geometry parameters of the package are unchanged. The results are shown in Fig. 11. There exists a nonlinear relationship between the fatigue life of the solder joint and the die thickness. In addition, from Fig. 11, the fatigue life of the solder joint increases as the chip gets thicker. However, the maximum discrepancy is trivial and is less than 1%.

2. Effect of Die Size

In order to examine the dependence of the die size on the solder joint’s fatigue life, nine different die sizes are used in the parametric analysis. The
results are shown in Fig. 12. It shows that the fatigue life of the solder joint is considerably varied with respect to die size variation. The fatigue life is initially enhanced as the die size is enlarged. As soon as the die size crosses 7.0 mm, it decreases while the die size increases. The maximum discrepancy can be up to about 40%. The 7 mm die size will provide an optimal reliability design for the assembly.

The trend of the results contradicts those of the face-up PBGA assembly derived from Ju et al. (1995). Ju et al. reported that the most reliability-concerned solder joint is located right beneath or the nearest to the chip since those are the largest CTE mismatches within the whole assembly. Accordingly, the larger the die size or die thickness, the shorter the fatigue life of the solder joint. For the cavity-down TEBGA assembly, the $x$-directional expansion of PCB is greater than that of the package due to the fact that PCB comprises a larger CTE in this direction than the package. In other words, the farther from the center of the package, the larger the deformation. As a result, the endmost solder joint will likely undergo the largest deformation.

However, according to Fig. 13, the above conclusions can only be partially true for the TEBGA assembly. It presents the relationship of the maximum average equivalent inelastic strain range versus the solder joints at different die sizes, i.e., 2.0, 6.0, and 10.0 mm, in which the value “1” in the abscissa represents the nearest joint to the die and “4” denotes the farthest one. As can be seen from Fig. 13, the maximum inelastic response for the 2.0 mm die size case occurs at Joint “4”. This result considerably justifies the preceding statements that the farthest joint will suffer from the highest inelastic strain response. On the other hand, for the 10.0 mm die size case in Fig. 13, Joint “1” becomes the most critical one. This implies that more than one thermal-mechanical deformation mechanism exists in the problem. Essentially, the dominant deformation mechanism for the 2.0 mm die size is due to the global CTE mismatch between the PCB and the package; however, for the 10.0 mm die size it is attributed to the local CTE mismatch between the die and the heat spreader. The result is consistent with Lee and Lau (1998).

3. Effect of PCB Thickness

A total of six FE models, each with a different PCB thickness, from 0.6 mm to 1.4 mm, are used in the parametric study. It should be noted that both the CTE and Young’s modulus are kept constant while the thickness of the PCB is varied. As shown in Fig. 14, the relationship between them turns out to be relatively linear, and more importantly, the fatigue life of the solder joint decreases as the thickness of PCB increases. This result shows exactly the same trend as that of the conventional cavity-up PBGA. The maximum decrement is about 10.0%. The increase is due to the fact that the increase of the PCB thickness will inevitably deepen its dissimilarity in
expansion with the die. It should be specially noted that this result provides the same tendency as that of the PBGA.

4. Effect of Die Attach Adhesive Thickness

As shown in Fig. 15, considerable linear relation exists between the fatigue life of the solder joint and the thickness of the die attach adhesive. There is about 4.0% decrease of the solder joint fatigue life as the thickness of the die attach adhesive increases from 0.015 mm to 0.095 mm. It should be specially noted that this result shares the same tendency as that of the conventional cavity-up PBGA, in which the larger the die attach adhesive thickness, the worse the solder joint reliability.

5. Effect of Heat Spreader Thickness

The influence of the thickness of the heat spreader on the solder joint reliability is presented in Fig. 16. It can be easily observed that there exists a relatively complicated deformation mechanism governing the solder joint response as the heat spreader thickness becomes relatively thin. The phenomenon may stem from both the local CTE mismatch between the chip and the heat spreader and the global CTE mismatch between the package and the PCB. However, as the heat spreader becomes thicker, the effect of the global CTE mismatch between the package and the PCB becomes dominant. The maximum distinction is about 5.0% as the heat spreader thickness changes from 0.15 to 0.45 mm.

6. Effect of FR-4 Young’s Modulus

It is reported by Yeh et al. (1996) that the Young’s modulus of the organic materials in the assembly may significantly vary from one supplier to the other. In this work, the effect of the Young’s modulus of the FR-4 materials on the solder joint reliability is investigated. There are about six to eight TEBGA models applied for the particular purpose, each of which contains a different Young’s modulus.

The parametric result is shown in Fig. 17. It indicates that the relationship of the solder joint fatigue life versus the FR-4 Young’s modulus is relatively linear. As the FR-4 Young’s modulus...
increases, the fatigue life of the solder joint decreases. In addition, there is a 20.0% discrepancy as the Young’s modulus of the PCB changes from 15.0 to 25.0 GPa. In addition, the trend of the parametric result to a large extent duplicates that of the PCB thickness shown in Fig. 14.

7. Effect of FR-4 CTE

In order to explore the effect of the FR-4 CTE on the solder joint fatigue life, six different packages, as shown in Fig. 18, are employed. The parametric result is presented in Fig. 18, in which a nonlinear relationship can be observed. More importantly, the results also demonstrate that the present design of the TEBGA assembly provides the best reliability performance of the solder joint such that the maximum fatigue life cycle is about 4000 cycles. In contrast to the other aforementioned geometry parameters, the effect of these material parameters on the solder joint fatigue life seems to be the most pronounced.

VII. CONCLUSIONS

Characterization of the fatigue life of the solder joints of a typical TEBGA assembly subjected to an accelerated temperature cycling load is performed by using a parametric FE analysis with respect to the chosen material property constants and geometry parameters. The geometry profile of the solder joint within the package is extensively predicted using the Surface Evolver. It is found that the strain response near the geometry/material singularity point can be substantially characterized using the finite-volume-weighted averaging technique and more importantly, would no more be mesh-sensitive when the mesh is adequately dense. Furthermore, from the data of the computer modeling and the experimental validation in Table 2 and 3, one can detect that there exists some significant discrepancy in the data derived from the 2-D plane strain modeling and the measured. On the other hand, a very close and satisfactory agreement is obtained between the measured and the 3-D solid modeling. These imply that the thermal-mechanical behaviors of the materials applied in the assembly are appropriately defined. Even though the 2-D plane strain modeling could not provide results as accurate as the 3-D solid modeling, as long as the analysis model is extensively verified and the corresponding failure mechanisms would not be varied, it would be still preferable in the current parametric study due to its cost-effective features. From the parametric design, the impact of these design parameters on the solder joint reliability ranges widely. The effect of the CTE of the organic materials, PCB, significantly dominates the other factors. Conclusively, the design guidelines for obtaining optimal reliability performance of this typical TEBGA assembly can be deduced as follows: a larger die thickness, a thinner PCB/die attach adhesive/heat spreader, a smaller FR-4 (PCB) Young’s modulus, a 7.0 mm die size together with a currently employed FR-4 CTE.

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NOMENCLATURE

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REFERENCES


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加強熱導型球柵陣列電子構裝錫球疲勞壽命影響
因子分析

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摘 要

本文藉由有限元分析技術，探討一加強熱導型球柵陣列電子構裝於承受一極為嚴苛之熱加速循環載重下，構裝各元件之幾何及材質對錫球疲勞壽命之影響因子。為節省計算成本，本文將採用一平面應變有限元分析模式。爲確認此一數值分析模式及其元件之非線性材料行爲的有效性，分析求得之結果將藉由三維實體有限元分析模式及一搭配雲紋干涉儀之干涉位移量測技術加以驗證。爲求能精確描述錫球之熱應變乃至疲勞壽命，回鋸後之錫球幾何外形將藉由能量法（見 Brakke, 1994; Chiang et al., 2000）加以分析獲取。另一方面，上述之分析模式牽涉到幾何及材料之不連續性而常導致應力／應變之集中現象，因此本文提出一有限體積加權平均法以冀求能有效描述於此一區域之機械行爲。分析求得之錫球熱應變將搭配 Coffin-Manson 法則以有效預估錫球之疲勞壽命。

關鍵詞：加強熱導型球柵陣列構裝，疲勞壽命，有限體積加權平均法，參數化有限元分析，Coffin-Manson 法則，雲紋干涉儀。