

Durability of Lead-Free Solder Interconnections for Printed Circuit Board Applications: Comparing Energy-Based Thermo-Mechanical Fatigue Models

Chien-Ming Huang
Mechanical Engineering
University of Maryland
College Park, USA
cmhuang@umd.edu

Jeffrey W. Herrmann
Mechanical Engineering
University of Maryland
College Park, USA
jwh2@umd.edu

Abstract—Fatigue models for predicting the cycles to failure of solder interconnections under temperature cycling situations have been discussed and developed for decades. However, most models were developed for different solder materials, components, and printed circuit boards. No previous work has systematically compared these models. Therefore, the variability of their durability predictions is unknown. This study compared nine existing low-cycle energy-based fatigue models for different solder materials and components and then analyzed the differences among them. Each fatigue model had a specific combination of the factors that affect the strain energy density accumulation. Therefore, we adjusted the strain energy density (input) and the predicted cycles to failure (output) in a consistent way to compare the selected fatigue models on the same basis. The differences among the predictions on cycles to failure from the fatigue models was significantly reduced after applying the adjustments, and they exhibited excellent consistency around 1 mJ/mm^3 strain energy density. In the end, factors that can affect the prediction consistency of fatigue mode were provided, including the number of data points while building the fatigue model, the range of strain energy density while selecting the electronic components, and the application of volume-weighted averaging technique on the critical solder joint.

Keywords—lead-free solders, durability prediction, fatigue life, temperature cycling, printed circuit boards

I. INTRODUCTION

The durability of solder interconnections is critical in modern electronic products because the solder joints provide mechanical and electrical connections between chips, components, and printed circuit boards (PCBs). Solder interconnections can fail due to various loading conditions, including temperature cycling, vibration, or drop/shock impact [1]. Among these loading conditions, temperature cycling induced low-cycle fatigue is considered as one of the major failure mechanisms for the electronics.

The durability depends, in part, upon the choice of solder. Many lead-free solders have been developed during the past 20 years because of various considerations, including cost and reliability, and the component-level and board-level reliability and durability of different lead-free solders has been reported in the literature [2]–[4]. These results, which provide some insights about the performance of different solders, are based on experiments and finite element analysis (FEA) simulations with specific lead-free solders and assemblies.

An engineer who is developing a new product and wishes to use a specific solder material should conduct reliability tests

with prototypes of the new system. Performing the entire series of reliability tests can be time-consuming and costly, however. Therefore, FEA simulation tools, such as ANSYS, have become a popular technique to estimate the durability of solder interconnections in the electronics industry [5],[6] in order to obtain preliminary results without running costly tests. The results from such simulations depend upon multiple parameter values and modeling choices, however, and we are not aware of any systematic study of how these factors affect reliability estimates. This paper describes studies that we conducted to provide such information.

We considered how the solder material, the fatigue life model parameters, the use of volume-weighted averaging technique, and the parameters in the stress-strain model affect the expected number of cycles to failure (CTF) when using commercially available FEA software such as ANSYS. Our results show the inelastic strain energy density can be affected by the preferences of simulation methodology. Moreover, the limitations of some fatigue models are pointed out and the recommendations are provided for the engineers. These results will help engineers make better decisions when using modeling to support durability evaluation.

The remainder of this paper is organized as follows. Section II reviews related work and the selected fatigue models. Section III presents the analysis approach, and Section IV presents the simulation results, and Section V discusses the adjustment of durability predictions. Section VI concludes the paper.

II. RELATED WORK

For the fatigue models with cyclic strain energy input, both viscoplastic [7] and creep-only [8] energy models have been proposed. (Viscoplastic models include both creep and plastic energies.) In general, a damage model with an inverse power law form describes the relationship between the damage metric and cycles to failure. Damage metrics include accumulated viscoplastic and creep-only strain energy density, and the cycles to failure can be median life (50% failures) or characteristic life (63.2% failures in a Weibull distribution). Let N_f be the cycles to failure. Let D be the damage metric, and let A and n be the material constants, which can be affected by many factors, including solder materials, temperature cycling profile, and assembly structure. Equation (1) is a generalized fatigue life model for solder joint under temperature cycling.

$$N_f = (A \times D)^n \quad (1)$$

The cycles to failure of solder joint can be estimated by applying the power law (1) to the fatigue coefficient A , fatigue exponent n , and damage metric D . Many solder fatigue life constants have been proposed for SAC solder joint under various temperature cycling conditions [9],[10]. Table I lists the fatigue coefficients and exponents for solder joint fatigue life models from prior studies that determined these values by fitting the power law fatigue model to experimental data and simulation results. As shown in Table I, the damage metric in the fatigue model was categorized into two groups, which were viscoplastic strain energy density and creep strain energy density. This paper will discuss the prediction difference between these two groups later.

TABLE I. ENERGY-BASED LOW-CYCLE FATIGUE MODELS FOR SOLDER JOINTS

Prior Studies	Strain Energy Density	Fatigue Coefficient	Fatigue Exponent	Fatigue Life Prediction
Chen et al. [9]	Viscoplastic	1.05×10^{-5}	-0.59	N50%
	Creep	4.75×10^{-6}	-0.55	N50%
Syed [10]	Creep	1.90×10^{-3}	-1.00	N50%
Schubert et al. [11]	Viscoplastic	3.30×10^{-3}	-1.02	N63.2%
Hsieh [12]	Creep	4.15×10^{-6}	-0.60	N50%
Hsieh and Tzeng [13]	Creep	1.09×10^{-2}	-2.26	N63.2%
Ghorbani and Spelt [14]	Creep	2.70×10^{-2}	-2.27	N50%
Sun et al. [15]	Creep	4.38×10^{-8}	-0.39	N63.2%
Zhang et al. [16]	Viscoplastic	1.69×10^{-4}	-0.77	N63.2%

Many studies have worked on the fatigue life prediction of solder joints with strain energy density. Some models focused on viscoplastic strain energy density, whereas other models focused on creep strain energy density. When fitting the constants of fatigue model, utilizing different types of strain energy density would generate different numbers of the constants. Moreover, employing an established fatigue model without understanding its underlying assumptions can make significant deviation on the prediction of fatigue life.

In this study, the multilinear isotropic hardening model [9] was utilized to handle the plastic strain energy density and the Garofalo-Arrhenius creep model [17] was applied to estimate the creep strain energy density. The Garofalo creep model is composed of a hyperbolic sine equation with stress-dependent term and an Arrhenius equation with temperature-dependent term, as shown in (2), where $C1$ and $C2$ are coefficients related to the material, σ is applied stress, ϵ_{cr} is creep strain, $C3$ is stress exponent, $C4$ is activation energy divided by Boltzmann's constant, and T is temperature in Kelvin scale. Many studies have been published to establish data sets of Garofalo-Arrhenius parameters for lead-free solder materials [9],[18]–[20], and are summarized in Table II. For the convenience of discussion in this paper, a code name was used instead of the original name for each study.

$$d\epsilon_{cr}/dt = C1 \times [\sinh(C2 \times \sigma)]^{C3} \times \exp(-C4/T) \quad (2)$$

TABLE II. SUMMARY OF GAROFALO-ARRHENIUS CREEP CONSTANTS FOR SOLDER MATERIAL

Solder Alloy	Code Name	Garofalo-Arrhenius Parameters			
		$C1 (s^{-1})$	$C2 (MPa^{-1})$	$C3$	$C4 (K)$
SAC305 [9]	GA01	6.07	0.18	2.3	6,710
SAC387 [11]	GA02	277,984	0.02447	6.41	6,500
SAC405 [12]	GA03	1.15×10^6	0.0335	7.5	8,703.4
SAC105 [13]	GA04	2.31×10^6	0.026	6.5	6,962.7
SAC387 [14]	GA05	441,000	0.005	4.2	5,412
SAC387 [16]	GA06	1.5×10^3	0.19	4	8,575.9

The multilinear isotropic hardening property was utilized to model the time-independent plastic strain (ϵ_{pl}). The relationship between the applied stress and strain was addressed in (3) to (5) [9], where σ is the instantaneous stress, K and n_{pl} are temperature-dependent instantaneous plasticity material constants, and T is temperature in Celsius scale.

$$\epsilon_{pl} = (\sigma / K)^{1/n_{pl}} \quad (3)$$

$$K = 121.6 - 0.4 \times T \quad (4)$$

$$n_{pl} = 0.29 - 0.00046 \times T \quad (5)$$

Because the contribution of creep strain energy density is much higher than plastic one (around 10 times higher), this paper employed only one prior study for the multilinear isotropic hardening property, as addressed in (3) to (5), to simplify the comparison process.

III. APPROACH

To support the reproducibility of our work, we used a PCB design from IPC-2581, the online digital product model exchange that is a generic standard for the printed circuit board and assembly manufacturing description data. The test case for our study was IPC-2581 B Test Case 3 board design [21] with BGA168 component from ANSYS Sherlock part library. ANSYS Sherlock is a physics-based reliability tool that provides life predictions for electronics at the component, board, and system levels. Details about the dimensions and material properties are listed in Table III and Table IV. The values of Poisson's ratio for SAC305, BGA168, and the PCB, which were considered as isotropic, were 0.36, 0.20, and 0.15, respectively.

In our approach, we created the FEA model with ANSYS Mechanical APDL because it provided the flexibility to adjust the mesh of solder joints. In the FEA model, the geometry of the solder joint was built in a ball-shape. We then imported the FEA models into ANSYS Workbench for the FEA simulations. In order to consider the impact of the volume-weighted averaging technique, the number of temperature cycles, and the constitutive equations, we conducted multiple simulation runs using different combinations of values for these factors. Each simulation run yielded a value for the strain energy density ΔW with corresponding solder joint volume fraction, number of temperature cycles, and constitutive constants. In the end, we analyzed the variation of the cycles to failure prediction among the fatigue models. The results are given in the next section.

TABLE III. PARAMETERS OF THE BGA168 COMPONENT [21]

BGA168 Size	Ball Matrix	Number of Balls	Ball Diameter	Ball Height	Ball Pitch
13.5 × 13.5 mm	13 × 13 Full	168	0.645 mm	0.3 mm	1 mm

TABLE IV. MECHANICAL PROPERTIES OF MATERIALS IN BGA168 ASSEMBLY [21]

	Young's Modulus E (GPa)	CTE α (ppm/°C)	Shear Modulus G (GPa)
SAC 305	39.99	22.28	14.70
BGA168	25.08	9.70	10.43
PCB (x-y direction)	25.95	18.32	11.28
PCB (z direction)	7.05	57.53	11.28

Due to the nature of symmetry on the in-plane dimensions of the component, solder joints, and PCB, a quarter model of the FEA geometry was built to reduce the computational costs; the corresponding meshes are shown in Fig. 1.

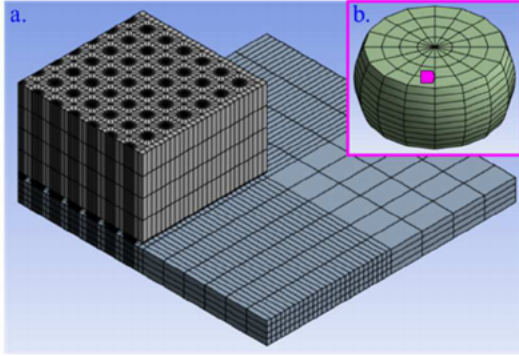


Fig. 1. Finite element meshes of BGA168 component and the PCB: (a) isometric view of the quarter model; (b) Solder ball

The strain energy density was determined with the volume-weighted averaging technique [22], which can use only a fraction of the elements in the calculation. We conducted simulations with different values for this fraction: 0.23% (critical element), 9%, 18%, 50%, and 100%. The corresponding volumes are shown in Fig. 2. The information of the volumes was obtained from ANSYS Workbench by inserting a Volume result object in the Solution.

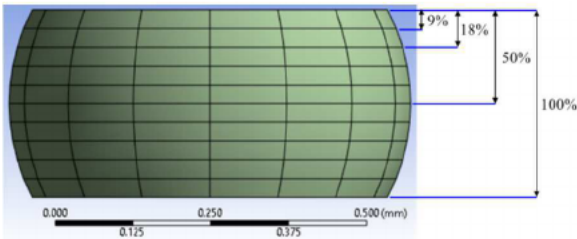


Fig. 2. A schematic of the number of elements selected for volume-weighted averaging technique

The cycles chosen for the accumulation of strain energy density can also affect the fitting of the constants of fatigue model. Fig. 3 showed the profile of temperature cycling used in this study. The value of this strain energy density accumulation (ΔW) can vary from cycle to cycle. Therefore, we examined the impact of using Cycle 1 to Cycle 4 accumulation to determine ΔW .

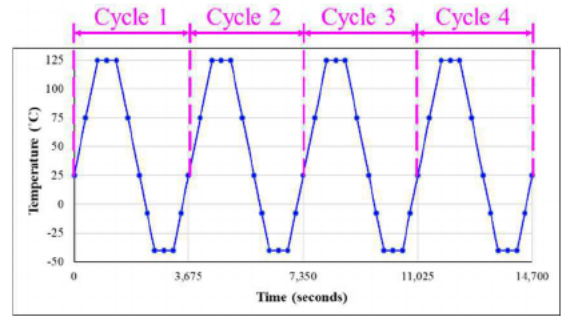


Fig. 3. Temperature cycling profile used in this study

Combing the abovementioned factors, the conditions of running simulations for the ball-shape solder joint were summarized in Table V. Our experiments employed a full factorial design, so there were $6 \times 5 \times 4 = 120$ combinations for the subsequent analysis.

TABLE V. CONDITIONS FOR THE FACTORS OF EXPERIMENT DESIGN

Factors	Level 1	Level 2	Level 3	Level 4	Level 5	Level 6
Constitutive model	GA01	GA02	GA03	GA04	GA05	GA06
Volume-weighted %	0.23%	9%	18%	50%	100%	---
Cycle accumulation	Cycle 1	Cycle 2	Cycle 3	Cycle 4	---	---

IV. RESULTS

The simulation results were summarized according to the solder volume and the simulated cycle, as addressed in Section III, in order to determine the necessary information for comparing the constitutive models and fatigue models. We used SAS JMP software to create variability charts (Fig. 4) to summarize the strain energy density data from all the considered situations and to compare the data by constitutive models, strain energy density types, cycles, and solder volumes.

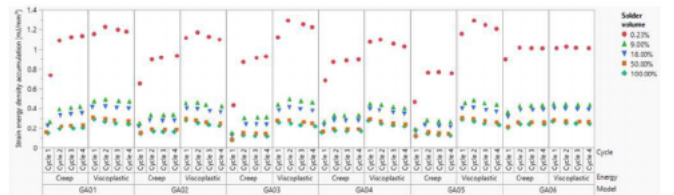


Fig. 4. Variability chart of strain energy density with the selected Garofalo-Arrhenius models

Fig. 4 can be interpreted from various perspectives. First, there were significant differences on the group means between 0.23%, 9% and 18%, as well as 50% and 100% solder volumes. Second, the strain energy density accumulation exhibited different behaviors for viscoplastic and creep energies. For the creep energy type, the energy accumulation of Cycle 1 was much lower than that of Cycles 2 to 4. On the contrary, the viscoplastic energy type displayed similar strain energy density values from Cycle 1 to 4. Third, the strain energy densities of viscoplastic energy type was higher than the ones of creep energy type for all constitutive models. However, the difference between the viscoplastic and creep energy type was not consistent among the models. Fourth, Student's t tests were conducted to compare the group means of each pair of the constitutive models for both viscoplastic

and creep energy type, there were no significant differences discovered among the constitutive models.

V. DISCUSSION

From Fig. 4, the effect of cycle accumulation on the strain energy density variation could be ignored for 9% to 100% volume-weighted conditions. For the 0.23% volume (maximum element value) application, the strain energy density variation was around 3.7%. Fortunately, the fatigue models that employed the maximum element value took the cycle accumulation after Cycle 4 except Schubert et al. [11], who didn't address the cycle accumulation, as shown in Table VI. Therefore, when comparing the fatigue models in this paper, Cycle 4 accumulation was employed for the greater than three cycles and not specified situations.

TABLE VI. STRAIN ENERGY DENSITY (ΔW) METHOD FROM THE SELECTED FATIGUE LIFE MODEL STUDIES

Constitute Model	Volume-Weighted Amount	Cycle Accumulation	ΔW from Our Simulations (mJ/mm ³)
GA01	8%	Cycle 4	0.47 (viscoplastic) 0.42 (creep)
GA02	5.6%	Cycle 1 or 2	0.33
GA02	100%	Not specified	0.22
GA03	Maximum value (element)	Cycle 5	0.93
GA04	Maximum value (element)	Cycle 5	0.90
GA05	Maximum value (element)	Cycle 10	0.76
GA02	9.3%	Not specified (performed 3 cycles in their simulation)	0.33
GA06	5%	Cycle 3	0.44

In Table VI, five of the fatigue models directly described the amount of volume-weighted for the critical solder or the maximum value of the critical element (no volume-weighted) in their studies [11]–[14],[16]. The remaining fatigue models [9],[10],[15] described only the thickness or the layer(s) of elements used for the volume-weighted technique. In this situation, the values in Table VI were determined by dividing the thickness of the layer(s) over the critical solder volume, which was calculated from the figure in each study. The volume of the critical element in each study was not possible to be approximated due to many reasons. Therefore, the 0.23% solder volume data was employed for the maximum element value. When the volume-weighted amount was less than 10%, the 9% solder volume data was applied for the subsequent comparisons because there should not exist a significant difference. Moreover, there were significant differences between the group means of 0.23%, 9%, and 100% solder volumes. For instance, the strain energy density of 0.23% volume was about 2.5 times higher than the one of 9% volume. Hence, the data from selected solder volume was critical while substituting into the fatigue models.

There was a significant difference on the group means between the viscoplastic energy group and creep energy group from the six studied Garofalo-Arrhenius models. Moreover, the difference between the viscoplastic energy and creep energy groups was not consistent among the six models. Therefore, it is important to be consistent with the energy type while fitting the constants of the Garofalo-Arrhenius model and to be careful when referring to the available Garofalo-

Arrhenius models. Based on all of these considerations, we determined that the strain energy density value needed to be adjusted before substituting it into the fatigue model. The strain energy density data from our simulations for each fatigue model situation was listed in Table VI.

Each fatigue model had a specific combination of factors (as shown in Table V) that affects the strain energy density calculation. For a given combination set, we found the ΔW by taking the accumulation of strain energy density from the cyclic strain energy density curve. Fig. 5 presents the process flow to calculate the adjustment ratio of strain energy density for each fatigue model. We first chose one of the fatigue life models, which was Chen et al. [9] with viscoplastic strain energy density, as the reference fatigue model A , ΔW_A . Then, for any of the other fatigue life models in Table VI, as the target fatigue model B . We repeated the simulation with the same temperature cycling profile, component, and PCB to get the target strain energy density, ΔW_B . For the creep constants of solder material, however, we used the values that were used in that study. Moreover, for each target fatigue life model, different settings of volume-weighted averaging amount and cycle accumulation was applied to determine the ΔW_B . Therefore, each fatigue life model had its own ΔW_B value. We then calculated the ratio R_B between each ΔW_B and the ΔW_A , as shown in (6). For the comparison purpose, these ratios would be employed for the subsequent discussions of the predictions of cycles to failure.

$$R_B = \Delta W_B / \Delta W_A \quad (6)$$

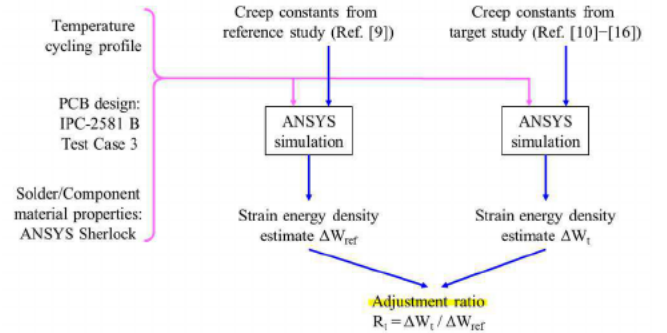


Fig. 5. Strain energy density adjustment flow for the target studies

Fig. 6 plots the published values of strain energy density (ΔW) and cycles to failure (N_f) on a log-log scale. The range of ΔW and the fatigue coefficients and exponents were taken directly from the literature (listed in Table I and Table VI), and then the cycles to failures were determined by using them with the inverse power law in (1) for each model from the literature. Most of the reported ΔW values covered only a small range, and the range was less than 1 mJ/mm³. However, this phenomenon could also reflect this small range might be a practical situation for modern electronic devices. For ΔW was greater than 2 mJ/mm³, fewer studies are available. Note that the strain energy density values are the results of simulations that used different sets of parameter values, and the cycles to failure values are different points (percentiles) from the empirical distributions collected by the different studies.

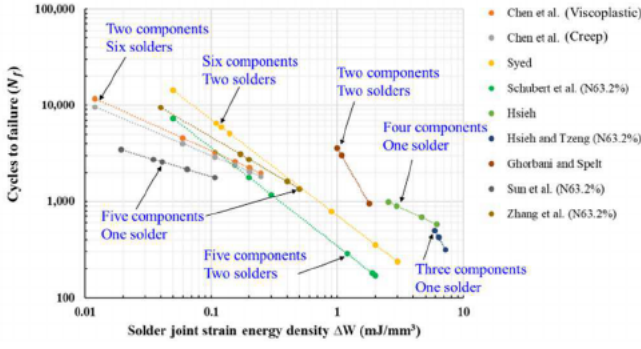


Fig. 6. Distributions of strain energy density from the selected fatigue life models and their predictions on the cycles to failure

In order to have a consistent comparison for all the selected fatigue models, five strain energy density values were chosen based on all of the combinations of our simulation results, which were $\Delta W_i = \{0.09, 0.575, 1.06, 1.545, 2.03\}$ mJ/mm³ for $i = 1, 2, 3, 4, 5$. These five values were used as the inputs of the fatigue models. Therefore, the predicted cycles to failure can be compared within the same strain energy density scope.

The strain energy density value, ΔW_i , was multiplied by that model's adjustment ratio, R_B , before applying the inverse power law, as shown in (7), to get the adjusted strain energy density, $\Delta W_i'$, for each target fatigue life model, where i was from 1 to 8 due to total eight target models. After that, the cycles to failure (N_f) can be predicted with the adjusted strain energy density and the inverse power law with the corresponding coefficient and exponent from each target model, as presented in (8).

$$\Delta W_i' = \Delta W_i \times R_B \quad (7)$$

$$N_f = (A_B \times \Delta W_i')^{\beta_B} \quad (8)$$

To have a more straightforward understanding of the predictions of all the selected fatigue models, the adjustment of life percentile was applied, as shown in (9) and (10). N_{fi}' was referred to values that were derived from different ΔW_i . If the target model predicted median life (N50%), N_{fi}' was the same as the N_f , which was the outcome of the inverse power law. On the other hand, if the target model predicted characteristic life (N63.2%), N_f was multiplied by $0.6931^{1/\beta_B}$ to get the adjusted N_{fi}' . β_B was the shape parameter of the two-parameter Weibull distribution from the field failure data in each target fatigue life model. We averaged that study's published values of the shape parameter β to determine an aggregate β_B for that study. The ratio $0.6931^{1/\beta_B}$ is the scaling factor from N63.2% to N50% in the Weibull distribution. The entire adjustment process flow for the cycles to failure prediction is shown in Fig. 7.

$$N_{fi}' = N_f \text{ (if predicting N50\%)} \quad (9)$$

$$N_{fi}' = 0.6931^{1/\beta_B} \times N_f \text{ (if predicting N63.2\%)} \quad (10)$$

The adjusted cycles to failure predictions were shown in Fig. 8. The differences of the predictions on cycles to failure among the fatigue models was significantly reduced after applying the adjustments and exhibited excellent consistency around 1 mJ/mm³ strain energy density.

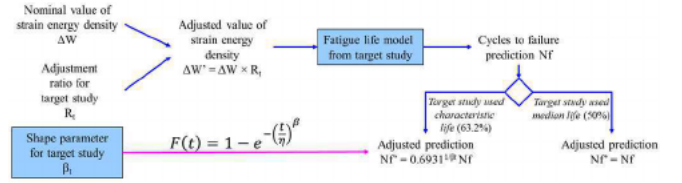


Fig. 7. Process flow of the adjusted prediction of fatigue model

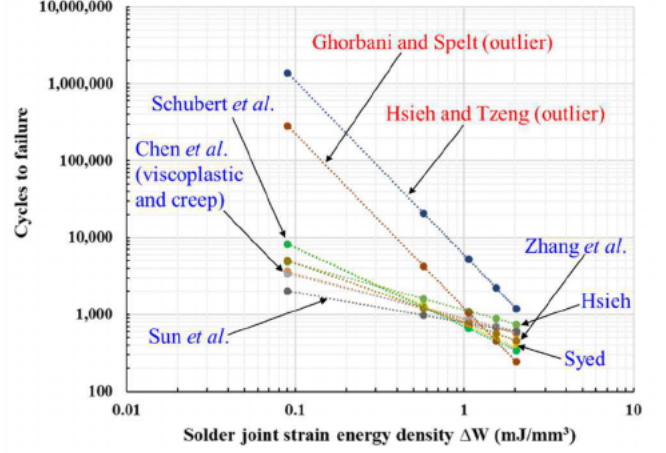


Fig. 8. Distributions of strain energy density with selected ΔW range: With ΔW and characteristic life (N63.2%) to median life (N50%) adjustment

Two of the fatigue life models, Hsieh and Tzeng [13] and Ghorbani and Spelt [14], exhibited divergences from the other seven models (see Fig. 8). Three factors were found that were responsible for the significant divergence. First, both models did not apply volume-weighted averaging technique for determining the strain energy density. They employed the maximum element value as the strain energy density (see Table VI). This could induce large variation on the strain energy density while changing the mesh size of the FEA model. Therefore, the adjustment method for the strain energy density (see Fig. 5) may not be appropriate for these two models because their mesh sizes were obviously smaller than our mesh size. Second, these two models only used three data points while curve fitting the coefficient and exponent of the inverse power law. This was the minimum number of data points for curve fitting an inverse power law, so predictions that were based on values of ΔW outside the range of their data can deviate in a certain amount. Third, the ΔW range selected by these two models was around 1 mJ/mm³, which was too small comparing to the reported ΔW range (from 0.01 to 8 mJ/mm³). Combining with the second point, the prediction of the cycles to failure with smaller ΔW values can produce a huge deviation from the other seven models. Due to these factors, these two models appear to be outliers among the total nine fatigue models.

VI. SUMMARY

Meteorologists use computer models to forecast the weather, and they use multiple models to estimate the range of what might happen. In the same way, engineers might use a set of fatigue models to get a range of durability predictions. If used inappropriately, however, the range of predictions might be misleading. This paper describes a study that provides some insights on how to use existing fatigue models.

This paper presented the results of nine selected low-cycle energy-based fatigue models that were built with their own

solder materials, assemblies, simulation methodologies, and field failure data to predict the median life or characteristic life of the solder joint.

The differences between the selected Garofalo-Arrhenius constitutive relationships from different solder materials were not significant on the accumulation of strain energy density. Other factors, including the amount of the critical solder for volume-weighted averaging technique, the selection of temperature cycle for strain energy density calculation, and the type of strain energy density (viscoplastic or creep), did make significant differences on the strain energy density accumulation. The percentile of predicted cycles to failure of the selected fatigue models were also not consistent. Therefore, adjusting the cycles to failure predictions was necessary after using the strain energy density as the input to the inverse power law equation.

For the adjustments, the strain energy density range (0.09 to 2.03 mJ/mm³) with five data points was chosen as the inputs of the selected fatigue models. The distribution of the predictions on cycles to failure among the fatigue models was significantly reduced after applying the adjustments, and exhibited excellent consistency around 1 mJ/mm³ strain energy density. Two of the fatigue models still showed large deviations from the other models, especially for small strain energy density (less than 1 mJ/mm³).

For engineers who wish to use multiple fatigue models to get a range of durability predictions, our results suggest the following: First, choose fatigue models that were constructed using at least five data points. Second, select fatigue models that cover more than 1 mJ/mm³ of strain energy density. Third, use fatigue models that apply a volume-weighted averaging technique on the critical solder to avoid extreme values of the strain energy density.

REFERENCES

- [1] Kim, Y.K. and Hwang, D.S., 2015. PBGA Packaging Reliability Assessments under Random Vibrations for Space Applications. *Microelectronics Reliability*, 55(1), pp.172-179.
- [2] Singh, B., Menezes, G., McCann, S., Jayaram, V., Ray, U., Sundaram, V., Pulugurtha, R., Smet, V. and Tummala, R., 2017. Board-Level Thermal Cycling and Drop-Test Reliability of Large, Ultrathin Glass BGA Packages for Smart Mobile Applications. *IEEE Transactions on Components, Packaging and Manufacturing Technology*, 7(5), pp.726-733.
- [3] Osterman, M., 2018, September. Modeling Temperature Cycle Fatigue Life of Select SAC Solders. In *Proceedings of SMTA International*. Online available: <https://web.calce.umd.edu/lead-free/SMTA2018-Osterman.pdf>
- [4] Coyle, R., Johnson, C., Hillman, D., Pearson, T., Osterman, M., Smetana, J., Howell, K., Zhang, H., Silk, J., Geng, J. and Daily, D., Enhancing Thermal Fatigue Reliability of Pb-Free Solder Alloys with Additions of Bismuth and Antimony. *Proceedings of SMTA International*, Sep. 28 - Oct. 23, 2020. pp. 339-354.
- [5] Schubert, A., Dudek, R., Walter, H., Jung, E., Gollhardt, A., Michel, B. and Reichl, H., 2002, May. Reliability Assessment of Flip-Chip Assemblies with Lead-Free Solder Joints. In *52nd Electronic Components and Technology Conference 2002*. (Cat. No. 02CH37345) (pp. 1246-1255). IEEE.
- [6] Tee, T.Y., Ng, H.S. and Zhong, Z., 2006. Board Level Solder Joint Reliability Analysis of Stacked Die Mixed Flip-Chip and Wirebond BGA. *Microelectronics Reliability*, 46(12), pp.2131-2138.
- [7] Morrow, J., 1965. Cyclic plastic strain energy and fatigue of metals. In *Internal friction, damping, and cyclic plasticity*. ASTM International. Lazan, B.J. (edited). West Conshohocken, PA, USA.
- [8] Dasgupta, A., Oyan, C., Barker, D., and Pecht, M. (June 1, 1992). Solder Creep-Fatigue Analysis by an Energy-Partitioning Approach. *ASME. Journal of Electronic Packaging*, June 1992; 114(2): 152-160.
- [9] Chen, D.Y., Osterman, M. and Dasgupta, A., 2020. Energy based modeling for temperature cycling induced tin silver copper solder interconnect fatigue life. *Microelectronics Reliability*, 109, p.113651.
- [10] Syed, A., 2004, June. Accumulated creep strain and energy density based thermal fatigue life prediction models for SnAgCu solder joints. In *2004 Proceedings. 54th electronic components and technology conference (IEEE Cat. No. 04CH37546)* (Vol. 1, pp. 737-746). IEEE.
- [11] Schubert, A., Dudek, R., Auerswald, E., Gollhardt, A., Michel, B. and Reichl, H., 2003, May. Fatigue life models for SnAgCu and SnPb solder joints evaluated by experiments and simulation. In *Electronic components and technology conference* (pp. 603-610). IEEE; 1999.
- [12] Hsieh, M.C., 2015, October. Modeling correlation for solder joint fatigue life estimation in wafer-level chip scale packages. In *2015 10th International Microsystems, Packaging, Assembly and Circuits Technology Conference (IMPACT)* (pp. 65-68). IEEE.
- [13] Hsieh, M.C. and Tzeng, S.L., 2014, August. Solder joint fatigue life prediction in large size and low cost wafer-level chip scale packages. In *2014 15th International Conference on Electronic Packaging Technology* (pp. 496-501). IEEE.
- [14] Ghorbani, H.R. and Spelt, J.K., 2007. An analytical elasto-creep model of solder joints in leadless chip resistors: part 2—applications in fatigue reliability predictions for SnPb and lead-free solders. *IEEE Transactions on Advanced Packaging*, 30(4), pp.695-704.
- [15] Sun, W., Zhu, W.H., Danny, R., Che, F.X., Wang, C.K., Sun, A.Y. and Tan, H.B., 2007, April. Study on the board-level SMT assembly and solder joint reliability of different QFN packages. In *2007 International Conference on Thermal, Mechanical and Multi-Physics Simulation Experiments in Microelectronics and Micro-Systems. EuroSime 2007* (pp. 1-6). IEEE.
- [16] Zhang, Q., Dasgupta, A., Nelson, D., and Pallavicini, H. (January 6, 2005). "Systematic Study on Thermo-Mechanical Durability of Pb-Free Assemblies: Experiments and FE Analysis." *ASME. Journal of Electronic Packaging*, December 2005; 127(4): 415-429.
- [17] Garofalo, F., 1965. Fundamentals of creep and creep-rupture in metals (Creep and creep rupture in metals and alloys, fundamental information for instruction and reference). NEW YORK, MACMILLAN CO., LONDON, COLLIER- MACMILLAN, LTD., 1965. 258 P.
- [18] Clech, J.P., 2003. Review and Analysis of Pb-Free Solder Properties. report to the NEMI Pb-Free Solder Project.
- [19] Cuddalorepatta, G. and Dasgupta, A., 2008, April. Effect of primary creep behavior on fatigue damage accumulation rates in accelerated thermal cycling of Sn3.0Ag0.5Cu Pb-free interconnects. In *EuroSimE 2008-International Conference on Thermal, Mechanical and Multi-Physics Simulation and Experiments in Microelectronics and Micro-Systems* (pp. 1-8). IEEE.
- [20] Jiang, Q., Deshpande, A. and Dasgupta, A., 2022. Multi-scale Crystal Viscoplasticity Approach for Estimating Anisotropic Steady-State Creep Properties of Single-Crystal SnAgCu Alloys. *International Journal of Plasticity*, 153, p.103271.
- [21] IPC-2581 B Test Case 3, IPC-DPMX (IPC-2581) Consortium. Online available: <http://www.ipc2581.com/b-test-cases/>
- [22] Akay, H.U., Paydar, N.H. and Bilgic, A., 1997. Fatigue Life Predictions for Thermally Loaded Solder Joints using a Volume-Weighted Averaging Technique. *Journal of Electronic Packaging*, 119(4), pp. 228-235.