Simple Methods for the Durability Assessment of Microelectronic Solders

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This paper presents a simple method, based on a 1-D model, that allows us to assess the impact of an essentially arbitrary thermal cycle test on the creep strain and energy dissipation in solder materials. Such tests are typically performed in order to assess the reliability and the lifetime of solders used for electrical and mechanical connection of microelectronic components. The method is applied to study the dissipated energy in terms of the stress-strain hysteresis in various solders, both lead-free as well as lead-containing. A first comparison between a full finite element study of the stress-strain hysteresis loop and the simplified approach is performed. It is shown that both lead to the same trends regarding creep strains and energy dissipation, however, the absolute values differ. For this reason a re-scaling procedure is proposed which allows to use the simplified approach for quantitative predictions.

Keywords Leadfree solder, creep, thermal cycle tests

1. INTRODUCTION

Modern Surface Mount Technology (SMT) uses various types of solder alloys for electrical and mechanical connection of a microelectronic component to a board. In general the Coefficients of Thermal Expansion (CTEs) of the board, of the materials that constitute the microelectronic component, and of the solder joint differ considerably. As a result thermal stresses will originate immediately after the solder has solidified. Due to the high ductility of the solders these thermal stresses should, in principle, quickly disappear. However, the microelectronic component will be switched on and off, electric energy will dissipate, local temperature will rise and fall, and the change in thermal stress will eventually fatigue the solder joint. In the end a crack will form making the solder joint mechanically and electrically inoperative [1, 2].

From a technological point of view it is important to know how long the solder connection will stay intact. It is for that reason that so-called Thermal Cycle or Thermal Shock Tests (TCT/TSTs) are performed [1, 2]. During such tests the board with the mounted components is subjected to controlled changes in temperature by switching from a high to a low temperature environment. After several hundred of such cycles the components are tested for electric functionality, some of the boards are examined destructively, e.g., by shearing off the components and recording the remaining shear strength, and/or cross-sections of the solder joints are examined under the microscope and analyzed for damage and cracks. Such a procedure is, of course, time-consuming and expensive [1, 2].

In addition to an experimental analysis it is customary to perform a Finite Element (FE) analysis and study the development of stress and strain in a solder joint during temperature cycling. In particular, the irreversibly accumulated equivalent creep strain and creep energy density in the solder joint are of interest. Once these are known the increments in equivalent creep strain, $\Delta \varepsilon_{eq, acc}^c$, and creep energy density, $\Delta W_{acc}^c$, during each temperature cycle are used to determine the number of life cycles, $N_f$, by means of empirical equations of the Coffin-Manson type [1, 2]:

$$N_f = a (\Delta \varepsilon_{eq, acc}^c)^b, \quad N_f = c (\Delta W_{acc}^c)^d. \hspace{1cm} (1)$$

In these empirical laws the phenomenological constants $a$, $b$, $c$, and $d$ are determined using experimental results for the (average) value of $N_f$. Clearly these constants depend on the type of solder and, consequently, the experimental fit must be performed for every new solder alloy composition. However, the idea is that the constants can be used to predict the lifetime resulting in a TCT different from the one that was used during the fit, and there is hope that they do not depend on the geometry of the microelectronic component originally considered. In other words it is assumed that it suffices to compute only the associated state of irreversible creep strain and creep energy density without performing new experiments. It is fair to say that this strategy has its flaws, that it did not completely come true and that, just as the experiments, the FE approach is cumbersome, time-consuming, and expensive.

In this paper a relatively simple approach is proposed which allows us to replace the complex FE analysis, at least if the
objective is a relative ranking of the severity of different TC and TS conditions regarding the damage they induce in a specific solder. Moreover, by means of this approach can compare the state of irreversible creep strain and creep energy density induced in various solders for a given TCT or TST can be compared. Finally, it will be shown how the results from this simple method can be “rescaled” to agree with the creep strains and energy densities predicted by an FE analysis of a certain microelectronic component subjected to a certain TCT or TST. This, to a certain degree, renders it possible to study other temperature conditions and solder materials quantitatively as well.

2. THEORY

Consider the one-dimensional situation shown in Figure 1, left. A column made of solder is clamped firmly between two walls so that the total change of length, $\Delta l$, viz., the total strain, $\varepsilon_{\text{tot}}$, and total strain rate, $\dot{\varepsilon}_{\text{tot}}$, due to a changing temperature, $T(t)$, vanishes:

$$\varepsilon_{\text{tot}} = \frac{\Delta l}{l} = 0 \Rightarrow \dot{\varepsilon}_{\text{tot}} = 0. \quad (2)$$

We now assume that the total strain rate can be decomposed additively into an elastic, a thermal, a time-independent plastic, and an explicitly time-dependent creep part as follows:

$$\dot{\varepsilon}_{\text{tot}} = \dot{\varepsilon}_{\text{el}} + \dot{\varepsilon}_{\text{th}} + \dot{\varepsilon}_{\text{pl}} + \dot{\varepsilon}_{\text{cr}}. \quad (3)$$

For the elastic part Hooke’s law in one-dimensional form yields:

$$\dot{\varepsilon}_{\text{el}} = \left( \frac{\sigma(t)}{E(T(t))} \right). \quad (4)$$

with a temperature and, therefore, time-dependent Young’s modulus, $E(T(t))$. Moreover the thermal strain is given by:

$$\dot{\varepsilon}_{\text{th}} = \left( \alpha(T(t)) \left[ T(t) - T_{\text{ref}} \right] \right). \quad (5)$$

where $\alpha(T(t))$ denotes the temperature dependent CTE and $T_{\text{ref}}$ stands for the stress-free reference temperature. A natural choice for this temperature is the solidification temperature of the solder from which the mounted component will be cooled down following a prescribed temperature cycle. If we assume the solder to be perfectly (linear) elastic there would always be (thermal) stresses present, in the component as well as in the solder, unless the solidification temperature is reached again during thermal cycling. However, we will treat the solder as a material susceptible to creep. In other words, if we cool the mounted component from the solidification point down to some other temperature and allow the solder to creep, i.e., to “relax” these stresses will decrease and disappear after a certain while. In this case this very temperature could be chosen as the stress-free reference temperature with respect to which the impact of other temperature and allow the solder to creep, i.e., to “relax” these stresses will decrease and disappear after a certain while. In this case this very temperature could be chosen as the stress-free reference temperature with respect to which the impact of follow-up cycles, i.e., the increment of plastic work or strain could be assessed. Indeed, this increment would be independent of the choice of reference temperature if only sufficient thermal cycles are considered which are used to form an average and which will then enter the lifetime Eq. (1). In this sense the choice of reference temperature is arbitrary. For simplicity we will use for $T_{\text{ref}}$ the solidification temperature of the various solders during the following calculations.

If we consider the plastic as well as the creep strain to be incompressible, assume isotropy for the elasticity tensor and for the thermal strains, as well as temperature independence of all elastic material parameters and the CTE, the time-independent equations of plasticity according to Prandtl-Reuss yield that the plastic strain rate in axial direction is given by ($S_d$ denote the components of the stress deviator, $\sigma$ is the applied stress in axial direction, i.e., the only component of the stress tensor, $G$ is the shear modulus and $E_P$ denotes the plastic tangent modulus which in the following relation is also assumed to be temperature independent):

$$\dot{\varepsilon}_{\text{pl}} = \frac{S_d}{1 + \frac{E_P}{3G}} \left( \dot{\varepsilon}_{\text{el}} - \dot{\varepsilon}_{\text{th}} - \dot{\varepsilon}_{\text{cr}} \right) = \ldots = \dot{\varepsilon}_{\text{tot}} - \dot{\varepsilon}_{\text{cr}}. \quad (6)$$

Finally the creep strain is given by the following empirical relation:

$$\dot{\varepsilon}_{\text{cr}} = C_1 \left[ \sinh \left( C_2 \sigma(t) \right) \right] C_3 \exp \left[ \frac{C_4}{RT(t)} \right], \quad (7)$$

where $C_1$, $\ldots$, $C_4$ denote four empirical constants characteristic of the secondary creep behavior of a particular solder material and $R = 8.314 \text{ kJ/(kgK)}$ is the universal gas constant ([11], Chapter 13). Hence, for a given set of material parameters and a prescribed temperature profile $T(t)$, the relations (2–7) result in a highly nonlinear Ordinary Differential Equation (ODE) which can be solved numerically to obtain the stress response $\sigma(t)$. It should be pointed out that in the following analysis we will neglect the contribution $\dot{\varepsilon}_{\text{pl}}$ from time-independent parameter.
J2-plasticity. This is due to lack of material parameters, in particular reliable data for the tangent modulus $E_T$, which typically stems from complete stress-strain curves measured at a stress or elongation rate fast enough so that creep does not contribute. A more detailed analysis of this contribution is left to future research. It should also be mentioned that, based on previous FE experience, the plastic strain contribution is usually small when compared to creep and, finally, for SnPb97:

\[ \varepsilon^{\text{pl}} + \varepsilon^{\text{th}} + \varepsilon^{\nu} = \left[ \frac{\Delta l(t)}{l} \right] \gamma. \]  

Moreover, instead of (4/5/7) we now write:

\[ \varepsilon^{\text{el}} = \frac{\sigma(t)}{E(T_0)}, \quad \varepsilon^{\text{th}} = 0, \]

\[ \varepsilon^{\text{cr}} = C_1[\sinh(C_2\sigma(t))] C_3 \exp\left[-\frac{C_4}{RT_0}\right]. \]  

3. MATERIAL PARAMETERS

Temperature dependent Young’s moduli were used during the simulations. For eutectic SnAg3.5 we found in [3] for the shear modulus:

\[ G(T) = \frac{E(T)}{2(1+\nu)} = \left[ 19310.3 - 69.0 \frac{1}{K} (T - 273 K) \right] \text{MPa}, \]  

and a (temperature independent) Poisson’s ratio $\nu = 0.32$.

For all SnAgCu (SAC) solders that were close to the eutectic concentration including those with further chemical additives the following linear fit was used (incl. data from [4, 5]):

\[ E(T) = \frac{50000 - (50000 - 47000) (T - 293 K)}{373 K - 293 K} \text{MPa}. \]

\[ E(T) = \frac{36000 - (36000 - 19000) (T - 223 K)}{423 K - 223 K} \text{MPa}, \]  

and, finally, for SnPb97:

\[ E(T) = \frac{25300 - (25300 - 18000) (T - 233 K)}{398 K - 233 K} \text{MPa}. \]

Reference [4, 5] also served as a source for the CTEs of SnAg3.5:

\[ \alpha(T) = \left[ 20.2 - (20.2 - 21.7) \frac{T - 293 K}{423 K - 293 K} \right] 10^{-6} \frac{1}{K}. \]  

(14)

eutectic SAC = SnAg3.8Cu0.7, SnAg3.9Cu0.6:

\[ \alpha(T) = \left[ 17.6 - (17.6 - 21.0) \frac{T - 293 K}{423 K - 293 K} \right] 10^{-6} \frac{1}{K}. \]  

(15)

Pb93.5Sn5Ag1.5 (used for Pb97Sn3):

\[ \alpha(T) = \left[ 27.7 - (27.7 - 30.1) \frac{T - 293 K}{423 K - 293 K} \right] 10^{-6} \frac{1}{K}, \]  

(16)

and for Sn 59 Pb40 Ag 1 (used for SnPb36Ag2/SnPb40):

\[ \alpha(T) = \left[ 24.0 - (24.0 - 26.9) \frac{T - 293 K}{423 K - 293 K} \right] 10^{-6} \frac{1}{K}. \]  

(17)

For several other solders TMA measurements were performed and fitted analogously:

\[ \alpha(T) = [A (T - 273 K) + B] 10^{-6} \frac{1}{K}. \]  

(18)

with suitable constants $A$ and $B$. Finally, the coefficients $C_1, \ldots, C_4$ of the hyperbolic sine creep Eq. (7) are listed in Table 1. They stem from various sources as indicated. Note that in the case of SnAg4.0Cu0.5 and SnAg3.5 creep laws different from Eq. (7) have been used:

\[ \varepsilon^{\text{cr}} = C_1 (\sigma) \exp\left[-\frac{C_4}{T}\right], \]

\[ \varepsilon^{\text{cr}} = C_1 \frac{G}{T} \left[ \sinh\left(C_2 \frac{\sigma}{G}\right) \right] C_3 \exp\left(-\frac{C_4}{T}\right). \]  

(19)

4. RESULTS

4.1. Analysis of Temperature Cycle Tests

Figure 2 presents the profiles of typical temperature tests that were used in the following simulations. The first viewgraph depicts a TCT with a ramp time of 10 minutes and a hold time of 30 minutes, or TCT10/30' for short. The second picture shows a TCT20/1020. The third picture refers to a TST with a very short ramp time of only 1 minute and a hold time of 30 minutes. All of the temperature cycles discussed so far operate between a minimum temperature of $-40^\circ C$ and a maximum temperature of $+125^\circ C$, respectively, which is characteristic of tests for portable electronic products. In contrast to that the fourth plot shows a profile between temperature levels of $-40^\circ C$ and $+150^\circ C$ characteristic of automotive standards. Moreover the
TABLE 1
Creep parameters used during simulations

<table>
<thead>
<tr>
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<th></th>
<th></th>
<th></th>
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<th></th>
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<tbody>
<tr>
<td>SnAg3.5</td>
<td>0.26</td>
<td>866.0</td>
<td>5.5</td>
<td>5802</td>
<td>[3]</td>
</tr>
<tr>
<td>generic eutectic SAC</td>
<td>2.77984 $\times 10^7$</td>
<td>0.02447</td>
<td>6.41</td>
<td>6499</td>
<td>[4, 5]</td>
</tr>
<tr>
<td>SnAgCu (eutectic)</td>
<td>7.925 $\times 10^5$</td>
<td>0.0356</td>
<td>6.0</td>
<td>8106.8</td>
<td>[7]</td>
</tr>
<tr>
<td>SnAg3.0Cu0.5</td>
<td>1.0 $\times 10^8$</td>
<td>0.1</td>
<td>8</td>
<td>10224</td>
<td>[5]</td>
</tr>
<tr>
<td>SnAg3.8Cu0.7</td>
<td>3.2 $\times 10^4$</td>
<td>0.037</td>
<td>5.1</td>
<td>6524.7</td>
<td>[6]</td>
</tr>
<tr>
<td>SnAg3.9Cu0.6</td>
<td>143.4</td>
<td>0.108</td>
<td>3.7884</td>
<td>7567</td>
<td>[3]</td>
</tr>
<tr>
<td>SnAg3.9Cu0.6</td>
<td>4.41 $\times 10^5$</td>
<td>5.0 $\times 10^{-3}$</td>
<td>4.2</td>
<td>5412</td>
<td>[8]</td>
</tr>
<tr>
<td>SnAg4.0Cu0.5</td>
<td>$2 \times 10^{-21}$</td>
<td>—</td>
<td>18</td>
<td>9996.4</td>
<td>[3]</td>
</tr>
<tr>
<td>SnAg1.3Cu0.2Ni0.05</td>
<td>9.0 $\times 10^4$</td>
<td>0.11</td>
<td>7</td>
<td>8058.7</td>
<td>[5/9]</td>
</tr>
<tr>
<td>SnAg1.3Cu0.5Ni0.05</td>
<td>3.0 $\times 10^4$</td>
<td>0.11</td>
<td>7</td>
<td>8058.7</td>
<td>[5/9]</td>
</tr>
<tr>
<td>SnAg2.7Cu0.4Ni0.05</td>
<td>2.0 $\times 10^7$</td>
<td>0.143</td>
<td>5</td>
<td>11787.3</td>
<td>[5/9]</td>
</tr>
<tr>
<td>97Pb3Sn</td>
<td>527.8 $\times 10^{-6}$ $\frac{E}{T}$</td>
<td>2240 $\frac{1}{E}$</td>
<td>3.8</td>
<td>3485.5</td>
<td>[10]</td>
</tr>
<tr>
<td>SnPb36Ag2/SnPb40</td>
<td>9.62 $\times 10^4$</td>
<td>0.087022</td>
<td>3.3</td>
<td>8110</td>
<td>[11]</td>
</tr>
</tbody>
</table>

FIG. 2. Typical temperature cycles considered.
FIG. 3. Stress vs. the sum of elastic and creep strain.

FIG. 4. Influence of the hold temperatures on the stress-strain hystereses.

FIG. 5. Quantification of dissipation involved for different TCTs and hold temperatures.
FIG. 6. Influence of hold temperatures on the energy dissipation in a TST1'30'1'.

FIG. 7. Influence of ramp time on the energy dissipation during various TCTs.

FIG. 8. Influence of hold time on the energy dissipation during various TCTs.
FIG. 9. Influence of upper hold temperature on the energy dissipation of TCT10'30'10'.

FIG. 10. Influence of lower hold temperature on the energy dissipation of TCT10'30'10'.

FIG. 11. Hysteresis curves for the solders of Table 2 subjected to TCT20'10'20'.
FIG. 12. Dissipated energy densities for the solders of Table 2 subjected to TCT20°10'20°.

FIG. 13. FE calculations [12] for TCT45°0'45° and comparison with results from the simple model.
very long ramp time of 45 minutes is supposed to mimic in-field operating conditions.

Clearly all of these profiles are idealistic in the sense that linear ramps and constant hold temperatures were considered. However, with a little more effort it becomes possible to digitize more complex temperature dependencies. Typical real temperature profiles used for assessment of new lead-free solders are depicted in the fifth and sixth inset of Figure 2. Using them during the numerical solution of Eqs. (2/3/8) does not take any longer than considering one of the idealistic temperature-time dependencies. Even if this will not be exactly the temperature at a particular solder joint on a board the use of such curves has the advantage of capturing the dynamics of the temperature and its impact on the development of stress and strain.

Figure 3 allows the study of the development of a stress-strain hysteresis if the solder is subjected to a change in temperature. Specifically we have used the data for the generic SAC solder [4] subjected to a TCT10′30′10′. The deformation behavior, in particular the creep during hold times is clearly visible. Note that $T_{ref} = 221^\circ C$ has been used in context with Eq. (5). The area surrounded by the hysteresis loop will, in future work, serve as a measure for the dissipated energy density, e.g., it can eventually be used in combination with equations of the Coffin-Manson type shown in Eq. (1).

Figure 4 shows how the hold temperatures influence the stress-strain hysteresis. Obviously, the broader the temperature range, the higher the dissipation will be. This effect is studied in more detail in Figure 5. Clearly extending the hold time leads to higher energy dissipation during the whole thermal cycle than increasing the ramp times. Moreover a decrease in temperature has essentially the same impact as the same amount of increase in temperature. Figure 6 shows dissipated energy densities as they result during a TST1′30′1′ depending on the hold temperatures chosen. First note that the TST leads to higher energy dissipation than a TCT with the same hold time and same hold temperatures.
In other words this test is more severe and tends to induce higher damage in the solder. Moreover there seems to be an almost linear dependence of dissipated energy density on the temperature range. Figure 7 demonstrates that decreasing the ramp time while fixing the hold time increases the dissipation and, consequently, demonstrates the severity of shock test conditions. In fact when compared to the slow ramp times this effect can be as much as 25%. Figure 8 allows us to study the impact of an increase of hold time while keeping the ramp time constant. There is a slight increase. However, when compared to the effect of ramp times shown in Figure 7 the damage potential is much less. Figure 9 investigates how the upper hold temperature influences the dissipation. Note that these simulations were made for SnAg2.7Cu0.4Ni0.05 [5, 9] and not for the generic type of SAC [4] solder as before. As expected an increase of the hold time leads to a (considerable) increase in dissipation. Similarly if the lower hold time is increased while the upper one is kept at a constant level the dissipated energy will decrease: Figure 10.

Figure 11 presents the hysteresis curves for all of the solders shown in Table 1 when subjected to a TCT20°10°20°. Obviously for SnAg4.0Cu0.5, and SnAg3.5 the stresses turn out to be highest. Moreover, the lead containing solders 97Pb3Sn and SnPb36Ag2/SnPb40 show the highest ductilities, i.e., the largest strain ranges followed by SnAg3.0Cu0.5 and SnAg3.5. It also becomes evident that good knowledge of the creep properties in combination with the corresponding microstructure is important since the hysteresis curves of SnAg3.9Cu0.6 from sources [4] and [8] differ considerably despite the same composition.

Figure 12 allows us to compare the dissipated energy densities for all solders from Table 1. It turns out that SnAg3.5 shows the highest value followed by the lead containing solders, and SnAg3.0Cu0.5. Interestingly SnAg1.35Cu0.2Ni0.05, SnAg1.35Cu0.5Ni0.05, and SnAg2.7Cu0.4Ni0.05 show the lowest energy dissipation. However, a high or low energy density does not necessarily mean a low or high lifetime since the
constants shown in Eq. (1) depend upon the solder material. Measurements are currently underway [5] to obtain specific values for some of the solders discussed in Figure 12, and therefore, a detailed evaluation is left to future research.

Figure 13 presents stress-strain results stemming from a full finite element study performed for the solder joint of a microelectronic ceramic capacitor 0402 subjected to TCT45°0.45° [13] which can be compared with those from the simple method outlined in Section 2. In both cases hysteresis cycles were calculated for various solder materials. The FE mesh of CC0402 was generated on the basis of micro-sections and component specifications. Use was made of symmetry, i.e., only one fourth of the component was modeled. More specifically, the hysteresis shown in Figure 13 depicts the in-plane shear stress component vs. the in-plane shear strain component of the element highlighted in the inset of the figure during the third thermal cycle. Both types of analysis results have several features in common:

- The highest stresses are encountered for SACT (a 6-component alloy).
- The maximum stresses encountered for the generic eutectic SAC and for eutectic tin-lead are very similar.
- Lowest tensile stresses are encountered for SnAg2.7Cu0.4Ni0.05.
- Eutectic tin-lead shows the largest strain range followed by SnAg2.7Cu0.4Ni0.05.
- Eutectic tin-lead shows the lowest compressive stress values.

However, there are also differences. For example the hystereses of the FE simulation are situated differently with respect to the strain axis when compared to the results of the simple method. It should be pointed out that with the exception of eutectic tin-lead the same creep laws have been used in both techniques. However, the CTEs were similar but not identical. Also note that the absolute amount of strain predicted by the simple method is

![Graph](image-url)
Fig. 17. Prescribed strain.

Fig. 18. Stress-strain hystereses predicted for eutectic SnAg at different temperatures.

almost one order below the one obtained by FE, a fact that cannot simply be explained by the factor $\sqrt{2/3}$ converting shear strain to equivalent strain. The stresses predicted by both techniques are of the same order of magnitude though, the ones stemming from FE being a little smaller accounting for the resilience of the component on the board in contrast to a clamped solder column.

Figures 14 and 15 show the cyclic creep strain and the dissipated creep energy density per cycle for the two test conditions presented in the last two pictures of Figure 2, calculated with the simple method as well as by FE-analysis for a ceramic resistor CR1206 mounted on a PC-board. Our discussion will focus on the solders common to both methods, namely on SnAg3.0Cu0.5 and SAC. As mentioned above both creep strain and energy density of the FE simulations are larger in absolute terms. However, there are certain similarities as follows:

- The creep strain difference and creep energy density of SnAg3.0Cu0.5 are higher than those of
- The test with the peak temperature at 150°C leads to higher creep strains and energy densities compared to the one at 125°C.

In Figure 16 we have rescaled the creep strain and creep energy density data of the prediction by the simple method to agree with the one predicted by FE for SnAg3.0Cu0.5 for the 125°C test. We may say that by doing so all strain results agree quite well and even for the energy densities the agreement is fair.

4.2. Analysis of Tensile Tests

In order to investigate the temperature behavior of stress-strain hystereses in a load controlled experiment (cf., Figure 1, right) we concentrate on eutectic SnAg and prescribe a loading rate as shown in Figure 17. Figure 18 depicts the resulting hysteresis at different temperatures: 125°C, 25°C, −40°C, and −100°C. Obviously by lowering the temperature the stress-strain response will become more and more elastic. Note that the area occupied by the hysteresis, i.e., the dissipation induced by the

Fig. 19. Dissipated energy densities predicted for eutectic SnAg at different temperatures.
loading is temperature dependent and will assume a maximum at a certain point. This is investigated in Figure 19. Numerical “experiments” such as this one may serve for orientation in order to find the best testing temperature for inducing a maximum of damage in the solder which is not necessarily at room temperature.

In Figure 20 an attempt was made to investigate the impact of loading frequency on stress-strain hystereses. As expected creep is dominant at low frequencies. Clearly from an experimental point of view it is important to know the right frequency to induce either elastic or plastic behavior if the objective is to measure the corresponding material properties such as Young’s modulus or creep constants. As demonstrated the “numerical experiment” allows us to narrow down these frequency ranges provided a first estimate of the elastic and creep parameters is known.

Finally Figure 21 allows us to study how each of the solders is affected by the loading type shown in Figure 17. These results deserve to be compared with Figure 12, the difference being that now a constant temperature is applied. It turns out that SnAg3.5 no longer shows the highest energy dissipation nor do the lead containing solders, or SnAg3.0Cu0.5. In fact it is SnAg3.0Cu0.5 which now shows the lowest whereas other solders, such as, SnAg3.9Cu0.6 [8] and SnAg4.0Cu0.5, now experience the highest energy dissipation. Note that the material data input clearly determines the outcome of this analysis: For SnAg3.9Cu0.6 [3] and SnAg3.9Cu0.6 [8] the predicted dissipated energy densities are roughly 20% apart which could be attributed to different microstructures of the specimens that were used to determine the corresponding creep data. However, as in context with Figure 12, it should be mentioned that a high or a low energy density does not necessarily mean a low or high lifetime, respectively.

5. CONCLUSIONS AND OUTLOOK

This paper started with an overview on experiments that are currently performed to assess the reliability of microelectronic solders and presented extracts from a thermo-mechanical database which has recently been compiled from the literature as well as ongoing publicly funded research work (BMBF project LIVE) for various technologically important solders. This data, in particular, Young’s modulus and creep constants were then applied to two types of deformation studies as follows. First, a slender solder beam clamped at its ends and subjected to various temperature cycle conditions relevant to portable electronics as well as to automotive applications was considered. Based on the aforementioned material data the resulting stress-strain hystereses were studied in order to obtain some information on the dissipated energy. This information was then discussed in context with simple reliability equations of the Coffin-Manson type for durability and reliability predictions. Second, the same procedure was applied to a slender beam clamped at one end and subjected to a suitable tensile as well as compressive deformation at the other.

The results of these 1-D investigations were compared with more elaborate FE-studies of three-dimensional microelectronic components soldered to a PC-board. The objective is to support the FE-reliability predictions made for these objects by a simplified analysis.

Future work should include a more detailed study of the transferability of 1-D hysteresis results when compared to 3-D FE-studies. So far a factor of proportionality has been established for simple solder joint geometries, namely for those of a ceramic capacitor. Furthermore the impact of time-independent plasticity on the dissipated energy should be investigated. This was not done so far simply because of lack of the required materials data, in particular temperature dependent yield stresses and tangent moduli. There is hope that such information should be available for the new solders in the near future.

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