Physics-of-Failure Lifetime Prediction Models for Wire Bond Interconnects in Power Electronic Modules

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Abstract—This paper presents a review of the commonly adopted physics-of-failure-based life prediction models for wire bond interconnects in power electronic modules. In the discussed models, lifetime is generally accounted for by loading temperature extremes alone. The influence of the time spent at temperature on bond wear-out behavior and damage removal phenomena resulting from thermally activated processes is not addressed. The phenomenological considerations based on some unusual observations highlight the need for new approaches to wire bond life prediction models and thus motivate the proposal of a new time-domain damage-based crack propagation model.

Index Terms—Physics-of-failure model, power electronic module, reliability, wire bond.

I. INTRODUCTION

POWER semiconductor modules are the core components in most of the power electronic systems, playing a key role in delivering flexible and efficient energy conversion. The reliability of these integrated power devices is critical to the overall performance and life-cycle cost of a power electronic system and even the whole power system. Therefore, a detailed understanding of the factors influencing the reliability of power electronic modules is becoming an increasingly important topic.

The primary reliability concern in power modules arises from the fact that they consist of materials with different thermal expansion coefficients, which causes thermal mechanical stress in adjacent layers under time–temperature exposure. The performance of these modules is greatly limited by stress-induced failures in the packaging materials [1]. Fig. 1 illustrates a schematic multilayer structure of a typical Direct Bonded Copper ceramic substrate power module.

In such a module, semiconductor dies are solder mounted on electrically insulating substrate, which is in turn soldered to a base plate. Electrical connections are made to the bottom terminal of the semiconductor devices via conducting metallization layer on top of the ceramic substrates. Metal wires are ultrasonically bonded to connect the top terminals
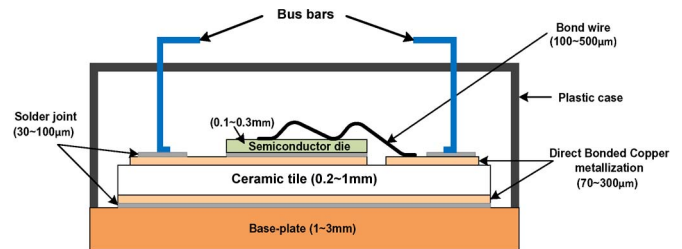
Fig. 1. Schematic cross section of a power module.

of the semiconductor device to external bus bars. The cavity of the shell is filled with silicone gel and is covered with a plastic, snap-on lid. Bond wires form the electrical connections between the semiconductor chips and the module substrate. Wire bond interconnect failure is one of the most important life limiting factors to the reliability of these modules [2]. Therefore, lifetime prediction for wire bonds is crucial to the reliability design and assessment of the whole module.

Selection of the approach to reliability design and prediction for electronic products has been an evolutionary and controversial process. Quality and reliability engineers used to adopt standardized stress-based methodology for qualification test and life assessment [3], [4]. Nowadays, reliability assessment based on physics-of-failure (PoF) knowledge is finding widespread application in power electronic products.

The PoF approach aims to identify the root cause of the potential failures and set up links between failure mechanisms and the lifetime under specified operation conditions, through the use of stress and damage models [5], [6], which facilitates a more meaningful life prediction approach. Moreover, lifetime behavior can be assessed with respect to new packaging materials, new configuration or manufacturing processes to provide information as to the improvements in their reliability. Therefore, the PoF-based methodology is not only a reliability prediction tool, but also a reliability enhancement technique. In addition, the application of PoF models in prognostics and health management allows the remaining life of an individual power module to be evaluated in the real-time operational environment and hence benefit system reliability and maintenance [1].

In the PoF methodology, the development of an effective physics-based model makes as critical a contribution to an accurate reliability prediction as the identification on the failure mechanisms, and remains the subject of extensive research for ultrasonic wedge bonds.

In the section which follows, the existing lifetime model representations in the literature, including simple acceleration models and detailed physical models are listed and briefly described. Their limitations are discussed in the subsequent section, with the view to highlighting areas which require further understanding and investigation. Modifications to existing methods are suggested and a new way of approaching wire bond lifetime assessment is proposed.

II. OVERVIEW OF STATE-OF-THE-ART LIFETIME PREDICTION MODELS FOR WIRE BONDS

Wire bond failure is largely attributed to thermomechanical fatigue caused by (1) repeated flexure of the wire (2) shear stress between bond pad and wire. These consequently induce vertical heel cracks or horizontal cracks at the bonding interface.

The most common plastic strain-based fatigue model is based on the Coffin–Manson relationship which defines a power law relation between number of cycles to failure N_f and the plastic strain induced per cycle ($\varepsilon_{\mathrm{pl}}$) for low-cycle fatigue [7], [8]

$$N_f = C_1(\Delta\varepsilon_{\mathrm{pl}})^{-C_2}. \qquad (1)$$

The value of plastic strain can be estimated by finite-element analysis (FEA) or by numerical calculation as a function of wire loop curvature. One of the earliest attempts at this approach to lifetime prediction of wire bond interconnects was illustrated in [2]. This approach can provide a useful tool for comparative evaluations and parametric studies of different materials and geometries. But the corresponding model validation can be difficult as the plastic strain is not a straightforward parameter to measure in experimental tests. In [9], the predicted lifetime of gold wire bonds was compared with the experimental results in [10] although the temperature input to the FEA model was different from the test temperature range. In [11], a temperature cycling experiment was conducted by means of liquid-to-liquid thermal shock to obtain a temperature variation from -40 °C to 125 °C. Here, strain was calculated for different bond loop geometries and a lifetime predicted for each case. Although the model produced the right trend, the cycles to failure it predicted were several orders of magnitude larger than the experimental results. The authors attributed the discrepancy between the lifetimes based on the calculated strains and actual wire bond lifetimes to the model not accounting for twisting and thinning of wires which would occur in real life. Furthermore, it was thought that the use of a power law-based calculation led to further overestimation of life.

In other instances, accelerated mechanical stress tests have been performed to simulate the strain generated by temperature fluctuation [12], [13]. However, this is not an adequate representation of the actual loading conditions for power electronic modules during operation.

Lifetime prediction where wire bonds suffer stresses below their yield stress (i.e., in the high-cycle regime) is usually based on Basquin's equation, [14], [15], in which stress range $\Delta\sigma$ is used as damage metric instead of plastic strain amplitude, as shown in (2)

$$N_f = C_3(\Delta\sigma)^{-C_4}. \qquad (2)$$

In [16] four such stress-based lifetime prediction models are presented for various failure mechanisms. A probabilistic approach has been employed to solve the uncertainty of temperature variation. However, the models are yet to be validated and only examples illustrating the application in the determination of dominant failure mechanism have been provided.

A similar model in [14] was validated by ultrasonic mechanical fatigue tests. Here, mechanical shear stress obtained by the special experimental setup was believed equivalent to those induced during temperature/power cycling or operational life. The mechanical fatigue results were in the region of 10^5 to 10^9 cycles to failure corresponding to an equivalent ΔT value between 30 K and 60 K. In [15], modules were stressed with a pulsed DC current of up to 2.5 A. Pulse lengths were between 10 ms and 100 ms at a duty cycle of 50%. Stress amplitude values were obtained from a 3-D FE model. However, it is worth noting that the simulated temperatures under the given current loading conditions reached as high as 700 °C. This would suggest error at some level with the simulations.

Another method for estimating the thermal fatigue life of wire bonds is based on fracture mechanics, wherein, crack growth per cycle is regarded to be governed by a stress or strain intensity factor, which controls the magnitude of the stress near the crack tip. Paris' law is generally employed to calculate crack growth rate per cycle (da/dN) using strain density factor range ΔK_ε

$$\frac{da}{dN} = C(\Delta K_\varepsilon)^n. \qquad (3)$$

C and n are material constants determined by comparing calculated and measured crack length. The strain density factor range is calculated with respect to temperature load by finite-element method. A critical crack length is selected as the criterion to estimate the number of cycles to failure. This type of model is mostly applied to crack propagation along the bonding interface.

An early attempt to apply fracture mechanics to life prediction for wire bonds in microscale electronic components was made in [30]. In a more recent paper [31], the fatigue lifetime of IGBT modules under small temperature ranges was investigated by this method. Temperature swings from 30 K to 100 K were set up in power cycle tests. The experimental results showed that the remaining bonded length reduced more rapidly for the bonds subjected to larger temperature ranges. The simulated thermal fatigue lives for these temperature ranges agreed well with the experimentally measured ones. It was inferred that fatigue lives for loading cycles with ΔT values less than 30 K would be virtually infinite. However, this assumption was not experimentally verified and would seem unlikely.

During application, wire bond interconnectors are subjected to temperature swings imposed by harsh external environments, power dissipation in the silicon die as well as the ohmic heat generation in the wire itself. Thermomechanical stresses usually result from mismatched coefficients of thermal expansion (CTE) between the wire (α_w) and the material to which it is bonded (semiconductor chip α_c). The resulting thermal

stress and equivalent strain can be expressed as a function of temperature swing

$$\varepsilon = (\alpha_w - \alpha_c)\Delta T. \tag{4}$$

Accordingly, to relate fatigue lifetime to the loading temperature range ΔT, (1) and (4) become

$$N_f = C_5(\Delta T)^{-C_6}. \tag{5}$$

In general, workers have found this model to accurately predict the lifetime of bond wires under thermal cycles where maximum temperature does not exceed 120 °C [17]. Therefore, it is commonly accepted that larger thermal cycling ranges result in shorter lifetimes. This theory has been widely used to predict the lifetime of wire bonds and also applied as a basis for bonding reliability improvement [18]–[21]. One such example is in [21], where it has been suggested that rearranging the position of bond wires on the chip interface could reduce their ΔT and thus improve their reliability.

It was proposed in [22] that in addition to ΔT, the absolute mean junction temperature T_m also had considerable influence on the lifetime characteristics of power modules. In this paper, power cycling tests were carried out at three mean temperatures (60 °C, 80 °C, 100 °C) and ΔT values from 30 K to 80 K were presented. A Coffin–Manson relationship was used to describe the dependence of number of cycles to failure N_f on temperature difference ΔT_j. A parallel shift observed for different mean temperatures T_m was assumed to be a simple thermally activated mechanism and was therefore expressed by an Arrhenius approach. The combination of two relations gave the (6)

$$N_f = A \cdot \Delta T^{\alpha} \cdot \exp\left(\frac{Q}{R \cdot T_m}\right). \tag{6}$$

The model in [22] was originally developed to evaluate the reliability of the whole module, and has since been employed in the reliability assessment of whole modules made with advanced packaging technologies for extended temperature ranges [23]–[25]. Moreover, their analysis indicated that bond wire lifting occurred before thermal fatigue of solder joints if ΔT was less than 130 K, and hence wire bond failure was regarded as the dominant failure mechanism during power cycling. Therefore, this relationship has been adopted by many researchers to evaluate the thermal fatigue lifetime for wire bonds [26], [27]. In [26], experimental validation was provided for (6) through fast thermal cycling tests with temperature swings from 50 K to 110 K.

Goehre *et al.* [18] argued plausibly that this approach is not best suited to wire bond lifetime prediction as the investigation on the degradation of wire bonds is not separated from other failure mechanisms. This is because their own experimental results showed that shear force degradation rate was mostly dependent on the amplitude of the temperature cycling and a significant effect arising from different mean temperatures could not be identified.

In addition to thermal loads, power devices inevitably suffer electrical stress during applications [28], [29]. In [29], failure mechanisms of a DMOS power switch under thermal and electrical overload conditions were investigated. The devices were stressed with periodic overload current pulses and a peak power dissipation in the range of $50 \sim 200$ W/mm^2. Pulsewidth was varied from 100 μs to 2000 μs and repetition rate from 20 ms to 2 s, to obtain peak junction temperatures between 200 °C and 350 °C. Severe degradation of the metallization and wire bond liftoff was observed. FIB (focused ion beam) cross sections across the failed devices showed that failures generally occurred at the edge of the bond wedge which is the location of the highest (critical) current density in the metallization layer. For the lifetime prediction, the Coffin–Manson model was modified to account for the influence of three physical stress parameters, namely temperature rise (ΔT_j), average junction temperature (T_{jave}) and critical current density (S_{crit}) at the bond location

$$NTF = \frac{A}{s_{\mathrm{crit}}^{2.2} \cdot \Delta T_j^{7.2}} \cdot \exp\left(\frac{0.58eV}{k \cdot T_{\mathrm{jave}}}\right) \tag{7}$$

where NTF is the median number of cycles to failure. This approach combines electromigration contributions with mechanical fatigue. However, in practice devices are rarely subjected to the kind of extreme overload conditions described in [29].

In [34], heel crack failure in aluminum ribbon bonds has been studied. An approach based on the estimation of the plastic strain energy was proposed to predict the ribbon lifetime during power cycling

$$N_f(I) = \frac{w_{\mathrm{pl}}^{cr}}{w_{\mathrm{pl}}(I)}. \tag{8}$$

Where $w_{\mathrm{pl}}(I)$ is the dissipated energy density in one ribbon corresponding to the current (I) in a stabilized cycle; w_{pl}^{cr} is the plastic strain energy density accumulated in a ribbon during its life. The electrothermal response of the ribbon was studied for current levels ranging from 30 A to 80 A per ribbon (2 mm $\times$ 200 μm). Electrothermal FEA was carried out to derive the temperature profiles. The temperature variation on the top of the ribbon loop was about 300 °C for an 80-A current. The dissipated energy density per power cycle was also obtained from FEA. Then the estimated results were compared with Coffin–Manson predictions and the authors deemed there to be good agreement between the two. However, although the approach produced a similar trend as the Coffin–Manson model, there were significant deviations between the predicted lifetime points. Furthermore, the energy-based estimations were not experimentally validated.

The above discussed models are essentially physical acceleration models. In [32], a fracture mechanics model was derived based on detailed wire bond physics. It was assumed that crack growth rate depended on the energy balance between the elastic strain energy released during crack growth and the energy required to create new crack surface area. The driving force for debonding was expressed in the form of strain energy release rate G as a function of wire material parameters and loop geometry.

$$G = \frac{3}{8}Eh^3\left(\frac{\Delta H_L^2}{a^4}\right) \tag{9}$$

where E is the elastic modulus of the wire material; h is the wire diameter; α is half the length of the bonding wire loop. ΔH_L is the local out-of-plane deformation of the wire bond under applied loads measured at loop midpoint, which can be measured by holographic measurements. The range of energy release rate ΔG is calculated with respect to the load range. The relationship between the rate of crack growth per power cycle da/dN and ΔG was obtained from the literature [33] which was originally achieved for the study of interface fatigue crack growth for Al/Al$_2$O$_3$. This model was tested for one loading condition ($\Delta T_j = 15$ °C) and hence requires further experimental verification.

III. SOME PHENOMENOLOGICAL CONSIDERATIONS FOR WIRE BOND LIFETIME MODELING

On the whole, in the existing models discussed, lifetime is generally accounted for by the loading amplitude alone, although this is represented in various forms. They share the common view that wire bond failure is the outcome solely of damage accumulation during repeated heating and cooling and the effect of duration of exposure to the temperature loads is not addressed. Therefore, the lifetime predictions made by these models lead to a common result that the number of cycles to failure decreases as the loading temperature range increases.

However, recent findings on the reliability of aluminum wire bonds under extended thermal cycling ranges have raised a number of interesting issues on wire bond lifetime modeling.

In [35], [36], the wear-out behavior of high-purity aluminum wire bonds under passive thermal cycling exceeding 125 °C was reported. The authors' initial intention had been to accelerate wire bond failures by using large ΔT values (180 K and 230 K) in order to generate lifetime data. For this reason, bonds were put through passive thermal cycling under the temperature ranges -55 to 125 °C (180 K), -35 to 145 °C (180 K), -60 to 170 °C (230 K) and -40 to 190 °C (230 K). However, measurements of the bond shear strength showed that the bonds subjected to higher maximum cycling temperature ($T_{\max}$) had slower wear-out rates (longer lifetimes) despite having larger temperature ranges (ΔT) (Fig. 2). Investigations of the fine scale microstructure and hardness of the bond wires showed noticeable softening and subgrain coarsening during the thermal cycling regimes with high $T_{\max}$ values and correlated annealing with a slower wear-out rate.

Findings in [37] on the reliability of thick aluminum wires bonded onto Si chips under three extended thermal cycling ranges, namely -40 to 150 °C ($\Delta T = 190$ K), -40 to 200 °C ($\Delta T = 240$ K) and -40 to 250 °C ($\Delta T = 290$ K) also showed similarly anomalous results. Here, despite large differences in temperature range of thermal cycle tests, the residual bond lengths of the wire bonds were almost the same (Fig. 3); in other words, crack propagation was not more rapid in bonds subjected to larger ΔT values, as (1), (2), or (5) would suggest. Moreover, EBSD (Electron Backscatter Diffraction) images of wire material revealed clearly that recrystallization and grain growth had progressed during the thermal cycling tests, and were most pronounced in those bonds exposed to the highest peak temperature.
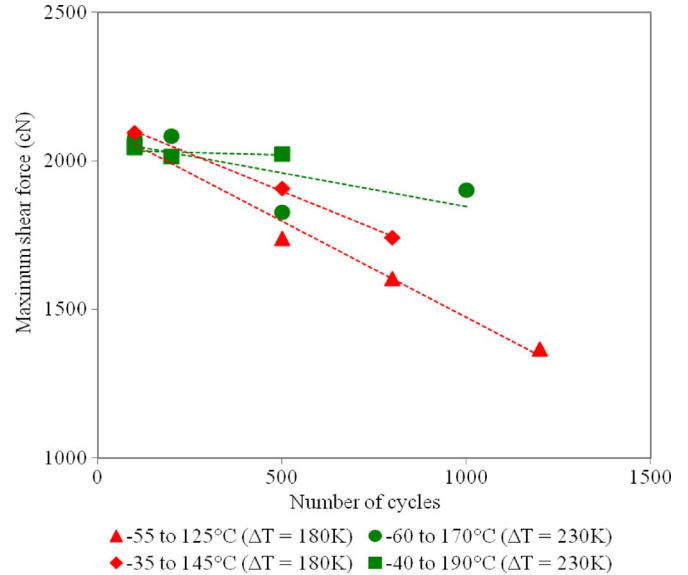
Fig. 2. Effect of increasing number of cycles on mean shear force [36].
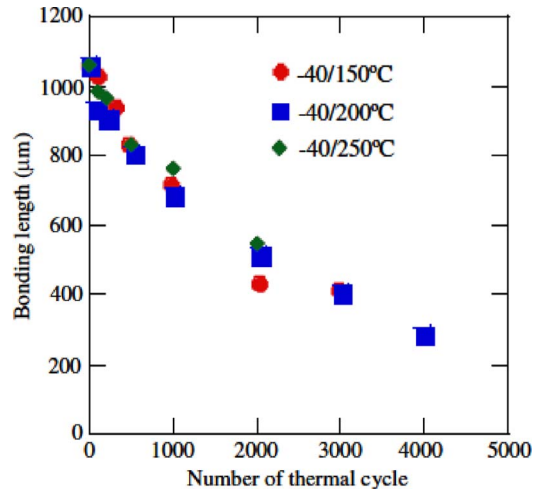
Fig. 3. Residue bonding length of aluminum wires during thermal cycling [37].

In [26], the effect of maximum temperature was investigated by varying it from 105 °C to 300 °C while keeping ΔT at a constant 80 K. Although a decrease in lifetime was observed for $T_{\max}$ values between 105 °C and 200 °C, no major change in fatigue characteristics of wire bonds was observed for $T_{\max}$ values between 200 °C and 300 °C (Fig. 4).

It is not difficult to extract several common findings from these observations: (1) when maximum peak temperature is high enough, the bonds exposed to the larger temperatures range either showed little difference [26], [37] or a slower deterioration rate [35] compared with those exposed to smaller temperature ranges. This apparently contradicts the Coffin–Manson law and would suggest that it cannot be applied universally. (2) Evidently, thermally activated processes occur during thermal cycling and are more significant at higher temperatures [14], [36], [37]. The occurrence of thermally activated phenomena such as recrystallization during thermomechanical fatigue is not in itself a controversial notion and has been widely reported even for isothermal fatigue conditions [14], [46], [47].
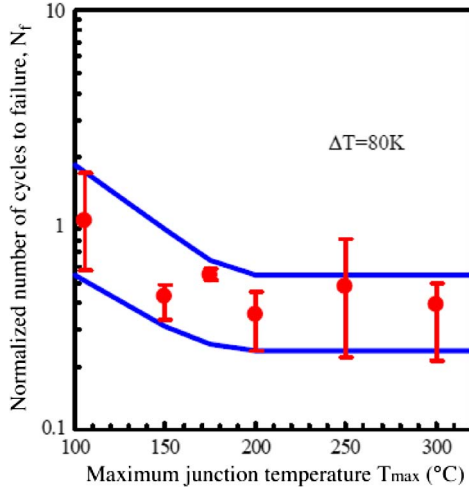
Fig. 4.   Relationship between N_f and maximum junction temperature [26].

Fig. 5.   Schematic graph of a wire bond foot.

However, its influence on failure is yet to be captured meaningfully by existing PoF models for wire bonds.

A number of disadvantages of the models discussed so far become apparent. These models generally focus on one mechanism as the cause of failure. This, we now recognize, is a rather simplistic view, because a broader wear-out mechanism such as wire bond liftoff can encompass a number of constituent mechanisms such as those which are diffusion driven, and whose interactions with fatigue can be complex under any given set of operation conditions. This also raises into question the use of accelerated testing, as there is a danger of these elements being overlooked or incorrectly represented. For example, if a fatigue-based wear-out mechanism, such as given by equations (1) and (2) is accelerated by temperature, then temperature-driven mechanisms, such as creep, which interact with fatigue are not accounted for, even though they are active under the acceleration conditions. Furthermore, because such models are material and mechanism specific, an enormous bank of life-testing data is needed. This is a huge snag, given that accelerated models are crucial to present-day assessment and qualification procedures.

In an attempt to address these issues, the authors put forward a damage-based prediction methodology in [35]. Since the observed grain coarsening and softening might be attributed to dislocation annihilation and a reversal of plastic strain [38], [39], it was concluded by the authors in [35] that "damage removal" and damage accumulation occur in tandem during thermomechanical cycling, and that the seemingly anomalous trend in bond degradation observed could be due to a dominance of the former at high-enough temperatures. Their model predicted degradation rate β of the bond shear force by introducing a variable D_1 to account for damage removal during exposure to elevated temperatures

$$\beta = \frac{D_1}{A\Delta T^{-M}}. \tag{10}$$

D_1 represents the proportion of damage remaining after a single thermal cycle. A and M are numerical constants usually determined experimentally.
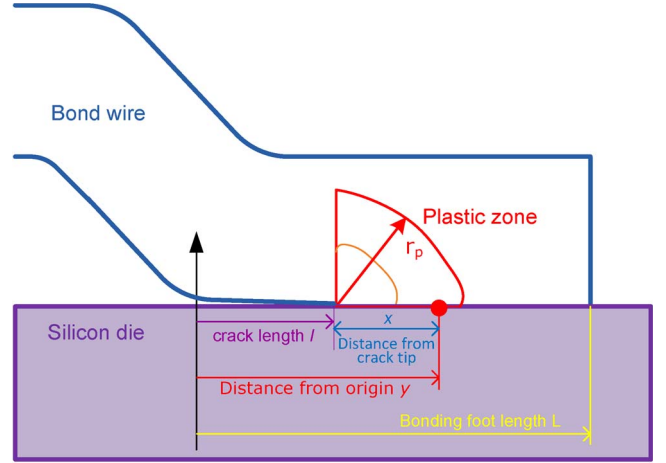
Although this model innovatively attempts to take account of the aforementioned damage removal effects on the bond degradation rate, the Coffin–Manson expression still forms the basis of its calculation of accumulated damage. Moreover, since the derived expression for D_1 is only valid for one particular temperature–time profile, this model cannot be readily extended to an arbitrary temperature–time profile.

Thus far, all of the models reviewed have been developed under the assumption that the bond wire is subjected to regular cyclic loading. In reality, however, power modules are usually subjected to irregular time-varying loads, either as a consequence of environmental changes or load cycling. Although the Rainflow counting method can be used to convert irregular time series into a sequence of cycles, this counting algorithm typically uses just the extreme points of the loads [40]. In [16] a probabilistic approach was employed to include the uncertainty of the temperature variation, but it just used a probability density function to statistically account for the temperature range history. In essence, cycle-based modeling methods are too simplistic and cannot readily describe the impact of time-at-temperature on the bond wear-out behavior.

IV. DAMAGE-BASED CRACK PROPAGATION MODEL IN TIME-DOMAIN REPRESENTATION

Consequently, an altogether new approach to wire bond life prediction models is needed which can account for the combined effect of damage removal and accumulation processes, as well as be able to predict life of arbitrary mission profiles. In such an approach, cycle-dependent methodologies may be replaced with a time-domain representation. In a previous publication [41], such a model has been presented in detail, and is summarized as follows.

A. Model Proposal

Fig. 5 shows a schematic diagram of a growing crack in a wire bond as a result of repeated heating and cooling. Damage accumulates at the crack tip and moves into the wire material to form a plastic zone. In the proposed model, a variable D is used to represent the "damage" condition in the wire material. It is

defined as a function of time and position through a differential equation which includes the effects of time- and temperature-dependent material properties.

Strain hardening and softening occur concurrently in bonding wire materials during temperature fluctuation [42]. Accordingly, the work hardening behavior of the material is represented by a damage accumulation term. Moreover, work hardening is influenced by temperature [43] and dynamic recovery and recrystallization which take place during thermal cycling [44], [45], evidenced by softening and subgrain growth. These thermally activated, time-dependent processes are also taken into account in the model via a damage removal term.

Thus the incremental damage at any position along the bonding interface can be described by the following general equation:

$$\delta D(y,t) = f(\varepsilon)f(D)f(T)\delta T - \alpha(D)\alpha(T)\delta t. \quad (11)$$

The position at the bonding interface is represented by y as the distance from the origin. The first term on the right-hand side of (11) accounts for the accumulation of the damage that is determined by the existing damage in the material D, the accumulated strain ε, and temperature T. The second term represents thermally activated damage removal processes dependent on the existing damage D, loading temperature T and the time t spent at this temperature.

$f(\varepsilon)$ is a strain distribution function representing the strain concentration factor at any position along the bonding interface

$$f(\varepsilon) = G_0 \left(-\frac{x}{\sqrt{LW} \, |\varepsilon_p|} \right). \quad (12)$$

The term $\sqrt{LW}$ defines the approximate bond foot length scale (L and W are the bond foot length and width, respectively); G_0 is a damage coefficient and ε_p is the plastic strain; x defines the position at the bonding interface with respect to the crack tip.

$f(D)$ is a work hardening term describing the damage accumulation process

$$f(D) = 1 + \alpha_H D^{\beta_H} \quad (13)$$

where α_H is the work hardening constant and β_H is the work hardening exponent.

$f(T)$ is a temperature-dependent damage term defining the dependency of accumulated damage on the loading temperature assuming that multiplication and annihilation reach equilibrium at temperature T_{eq}

$$f(T) = \left(\frac{T_{eq}}{T} \right)^{\beta_T} \quad (14)$$

where β_T is the temperature hardening exponent.

δT is represented by temperature-driven displacement strain $\delta\varepsilon_d$ due to the mismatched thermal expansion coefficients of the two bonded materials, assuming that the two bonded materials had the same length at the equilibrium temperature T_{eq}

$$\delta\varepsilon_d = \Delta CTE \cdot \delta T = (\alpha_w - \alpha_c) \cdot (T - T_{eq}) \quad (15)$$

α_w and α_c are the thermal expansion coefficients of the bond wire and semiconductor chip, respectively.

$\alpha(D)$ represents the existing damage.

$\alpha(T)$ is the Arrhenius expression to reflect the effect of temperature on the damage annihilation rate.

$$\alpha(T) = k_2' \exp \left(-\frac{Q}{RT} \right) \quad (16)$$

Q is the activation energy; R is the gas constant and k_2' is an annealing coefficient.

The total interface damage (D_T) at each time instant (t) can be calculated by the integration of the damage for all the points along the interface from the crack tip by (12)

$$D_T(t) = \int_{y=l}^{y=L} D(y,t)dy \quad (17)$$

where L and l represents the original bond length and crack length, respectively. Crack length l can be estimated by defining the crack growth rate as a function of total damage and the rate of change of total damage

$$\frac{dl}{dt} = f(D_T) + g \left(\frac{dD_T}{dt} \right). \quad (18)$$

Furthermore, decrease in bond shear strength can be attributed to a reduction in the bonded area as a crack grows. Hence, shear force is commonly used to quantify bond degradation, and can be empirically obtained for a given loading condition. The magnitude of shear force F_s can therefore be determined by the crack length l and the original bond foot length L

$$F_s = \left(\frac{L-l}{L} \right) F_0 \quad (19)$$

where F_0 is the initial shear force. The time to failure of the wire bonds can be estimated when the shear force reduces by certain percentage with respect to the initial value.

B. Some Simulation Results

1) Effect of Time-at-Temperature on Damage Development: One major advantage the proposed time-domain model has over cycle-based lifetime models is that it is able to more accurately reflect the temperature–time effects discussed previously. Moreover, these are represented in such a way that the model is not restricted to one material (e.g., aluminum). This is because the necessary work hardening and annealing parameters can be easily determined experimentally.

Fig. 6(a) and (b) show the evolution of the interface damage under two temperature profiles A and B. These two loading temperatures have the same temperature amplitude ranging from $-55\,^\circ$C to 125 $^\circ$C and the same heating/cooling rate of 6 $^\circ$C/minute. However, profile B has a 750-s dwell introduced at 125 $^\circ$C. The introduction of this dwell time at the maximum temperature is to illustrate the effect of time at temperature, i.e., more significant damage removal.

Fig. 7. Simulation of shear strength degradation of Al bonds under four temperature profiles.

TABLE I
COMPARISONS OF THE SHEAR STRENGTH DEGRADATION RATE BETWEEN EXPERIMENTAL DATA AND SIMULATION RESULTS

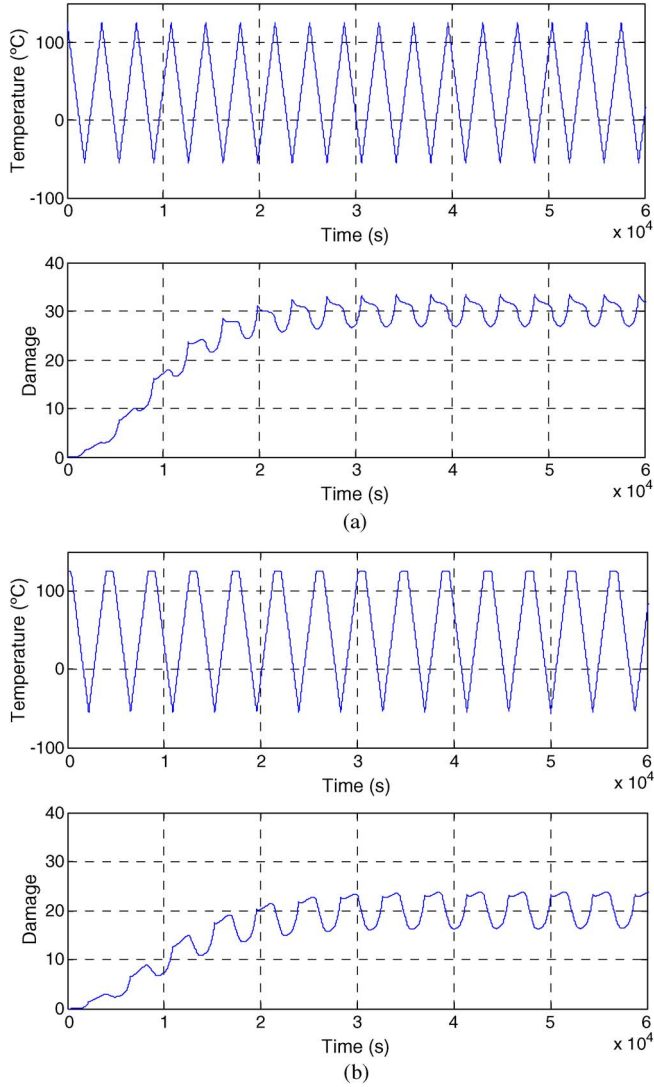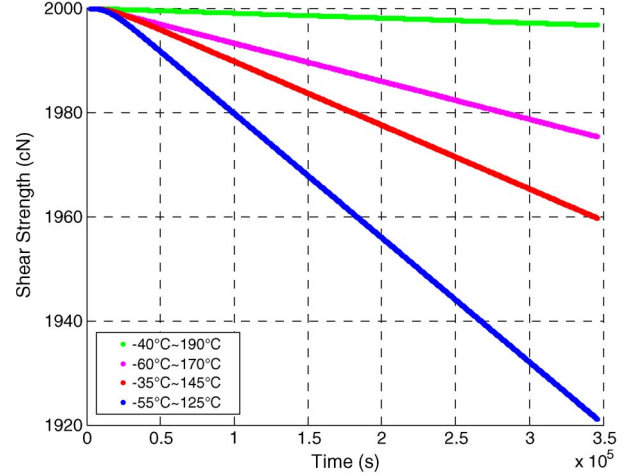| Temperature profile			Shear strength degradation rate	
T_{max} (°C)	T_{min} (°C)	ΔT (°C)	Experimental data	Simulation results
-55	125	180	-3.05×10^{-4}	-3.91×10^{-4}
-35	145	180	-2.35×10^{-4}	-1.99×10^{-4}
-60	170	230	-1.09×10^{-4}	-1.25×10^{-4}
-40	190	230	-1.66×10^{-5}	-1.66×10^{-5}

Fig. 6. (a) Temperature profile A and evolution of interface damage. (b) Temperature profile B and evolution of interface damage.

It can be seen that the damage develops at a different rate, and the saturation value of damage corresponding to profile B is smaller than that of profile A. This indicates that the model is able to demonstrate the effect of damage removal and time-at-temperature on the overall evolution of the damage.

2) Simulation of Bond Shear Strength Degradation: Evolution of bond shear strength is simulated by operating the damage-based crack propagation model under the four temperature loading conditions to which the aluminum bonding samples were subjected in the thermal cycling described in [36]. The simulated shear strength degradation lines are demonstrated in Fig. 7 based on time unit (second). To obtain a comparison with the experimental data which is evaluated per cycle, the simulation results of the wear-out rate were converted into the unit of per cycle. They are listed in Table I. These early results show good agreement between the experimental data and simulation results, and indicate that the model can describe these high-temperature related phenomena correctly. However, further experimental validation is required, and further work to link the functions $f_D(D)$, $f_T(T)$ and $\alpha_T(T)$ with measurable materials properties is still underway.

V. CONCLUSION

In this paper, a review of commonly adopted lifetime models for wire bonds is provided and their limitations are discussed with reference to observations in both our own experiments and those reported in the literature. These observations regard diffusion-driven damage removal mechanisms which are not accounted for and which lead to erroneous life predictions when current PoF models are employed. Other sources of error of current life prediction methodologies pointed out include the fact that they tend to be cycle based and thus cannot cope with realistic mission profiles which are more arbitrary in nature. A new way of looking at the problem is introduced, which involves a time-domain representation, and which can accommodate the aforementioned time-at-temperature effects. This approach is based on the estimation of the bonding interface damage condition at regular time intervals through a damage-based crack propagation model which includes the effect of material-dependent thermally activated processes. Other underlying factors which vary for different materials and which influence the buildup of damage, such as stacking fault energy are also taken into account implicitly by the incorporation of a work hardening term. Thus the impact of time at temperature and other rate sensitive processes on the bond degradation rate can be accurately represented. This way, the model accounts not only for the damage accumulation processes, but also the damage removal phenomena which may occur under certain operating conditions. Furthermore, it is thought that this approach should negate the need for extensive and laborious accelerated testing.

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