ENDURANCE OF SEMICONDUCTOR FUSES UNDER CYCLIC LOADING

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Summary

Current practice in selecting fuses for pulsed and cyclic loading duty is reviewed, and improved methods of selection are proposed, based upon the use of new thermal modelling methods and computer predictions of the temperature distribution within the fuse. Combination of the thermal predictions with a simple metal-fatigue model allows the prediction of life curves for arbitrary duty cycles. Each fuse can be characterised by a single factor which represents its ability to withstand cycling.

List of principal symbols

C_b natural convection loss constant
C_i thermal capacity of subvolume i, *J/deg C*
D effective diameter for convection loss, *m*
h surface power loss coefficient *W/m²/deg C*
K fuse fatigue constant
m,x exponents in fatigue equation
N number of cycles to fatigue failure
n time step index
P_{0i} "cold" value of P_i
P_i Joulean heating power in subvolume i, *W*
R_c thermal resistance (transient heat storage)
R_{ik} intervolume thermal resistance, *deg C/W*
[G] thermal admittance matrix
[q] vector of power terms
[θ] vector of subvolume temperature rises
α temperature coefficient of resistivity, *deg C^{-1}*
β lateral strain deflection factor
γ coefficient of linear expansion *deg C^{-1}*
Δt integration time step
Δθ p/p temperature fluctuation at hotspot, *deg C*
Δε maximum p/p strain fluctuation at hotspot
ϵ_b surface emissivity
ε total strain
σ Stefan-Boltzmann constant
θ_{av} average element temperature at hotspot, *deg C*
θ_i temperature rise of subvolume i, *deg C*

1. Introduction

Fuses are usually rated for continuous current duty, but fuses for the protection of power semiconductors usually carry a current which is not continuous, and selection of the correct fuse for a given application can only be made after the effects of pulsed and cyclic overloads [1] have been considered. Fuse standards usually require some tests to ensure that fuses have, in general, adequate ability to withstand pulsed and cyclic overloads, but very often the actual service duty is different from that which was used in the standard tests. In these cases recourse is usually made to "rules-of-thumb" in order to select the correct fuselink.

Computer simulation of cyclic loading behaviour requires large-scale modelling because of the need to follow both short-duration and long-duration transients. An approach to this is described which uses finite-difference methods formulated in terms of the RC-network analogue and solved using sparse matrix methods with ordered elimination. Reliable methods of dealing with the non-linear boundary conditions are also given.

The construction of a typical fuse for the protection of semiconductors is illustrated in Fig.1. There are one or more silver or copper notched elements, with no M-effect, within a sand-filled cartridge. Problems of long-term ageing due to the use of M-effect [2] require a separate approach, and are not considered here.

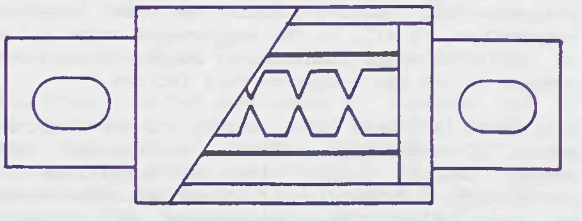

Fig. 1 Typical semiconductor-protection fuse

2. Rules-of-thumb for fuse selection

The first type of overload to be considered is the non-repetitive pulsed overload, or "occasional" overload [1]. This occurs only a few times during the service life of the fuse. Manufacturers usually recommend the selection of a fuse such that the current during the pulse does not exceed say 75% of the current which would cause operation of the fuse in a time equal to the duration of the pulse, according to the fuse's time-current characteristic. In fact, the fundamental requirement should be that the maximum temperature of the fuse element should not exceed a certain level, and this is not guaranteed by use of a fixed percentage-current value. Some of the difficulties which can arise by using a fixed percentage-current have been pointed out by Newbery [3], and often in practice the percentage allowed is varied in the light of experience for different fuse designs and overload conditions.

The second type of duty to be considered is the repetitive or cyclic load. Fuse selection in these cases is usually made on the basis that

(a) the r.m.s. value of the current over the whole cycle is below the rated fuse current, and

(b) if a sliding time-window of width T is moved across the fuse current waveshape, the r.m.s. current over the time T must not exceed a certain percentage of the current to produce melting in a time T, according to the time-current characteristic, and this must be true for all values of T.

In this case the percentage current allowed is significantly lower than for occasional overloads, typically 50% or less according to the fuse design and the nature of the load cycle. The relationship between the percentage current and the physical phenomena which may cause fuse deterioration in this case is much less clear. It is necessary to ensure that the peak temperature reached by the fuse-element during cycling does not exceed a certain level - a condition similar to that required for a single pulse. However, a more important requirement is to guard against mechanical fatigue. Semiconductor-protection fuses necessarily have very narrow notch zones, and repeated heating and cooling can give rise to fatigue failures if the temperature excursions during cycling are too high [4]. This is a thermo-mechanical process which is only indirectly related to the time-current characteristic.

It is possible to produce cyclic load withstand characteristics giving the number of cycles to failure as a function of current for fuses under given loading conditions and use these to predict the expected life [1], but it is difficult to modify these characteristics for different duty cycles. Furthermore extensive testing is required to evaluate each new fuse design if this approach is used.

Just as the continuous current rating is affected by environmental factors such as the ambient temperature, so will be the performance under pulsed or cyclic loading conditions, making it even more complex to use percentage-current factors.

Thus there is a need for a simpler and more accurate method of assessing a fuse's performance under pulsed and/or cyclic loading conditions. A prerequisite for this is a fast and accurate method of calculating the steady-state and transient temperature distributions within the fuse. A method for doing this is described in the next section.

3. Computer modelling of fuse thermal behaviour

The simplest model of fuse thermal behaviour is that it can be represented by a fixed thermal impedance and a single thermal time-constant. Whilst this may be useful for investigating general trends [5],[6] it is not sufficiently accurate for our purpose. When a real fuse is heated, there are very fast thermal responses, in the sub-millisecond region, associated with the notch zones. These are followed by phenomena in the 0.1-1s region, associated with transient losses from element to filler; in the 1-10s region, associated with heat loss along the elements to the ends; followed by the much slower responses, over half an hour or longer, of the bulk of the fuse filler, the body, and the connecting cables. A single time-constant is not sufficient to model all these phenomena.

For accurate calculations it is necessary to use numerical solution methods based upon finite-differences or finite-elements [7],[8],[9], but the application of these methods is not straightforward. The set of equations which results from the application of these methods is non-linear, because of the nature of the convective and radiative heat-loss from the fuse body, endcaps and cables to the surrounding medium. If iterative methods are used to solve these equations convergence is frequently impossible [10], and recent work has shown that for transient studies, iterative methods can fail even if the equations are forcibly linearised. For some studies, simplifying assumptions can be made (e.g. neglect of heat loss to the filler for short-times), but in the case of cyclic loading there are very few simplifications that can be made. Heat transfer through the whole fuse to the surroundings needs to be calculated to determine the background temperature distribution, upon which fluctuations are superimposed due to the cycling. To model these fluctuations accurately, the transient losses to the filler near to the element need to be calculated [11], requiring a large number of nodes in this region of space, together with a model of the notch zones with sufficient resolution to follow the rapid temperature transients in these zones. The net result of these requirements is that a model with around 10,000 nodes is needed, and since iterative solution methods cannot be used, it is essential to exploit the sparsity [12] of the system of equations.

3.1 General method

The method described here is fundamentally a finite-difference method but there are considerable practical advantages in formulating this in terms of an equivalent RC network analogue [13]. The fuse and its connecting cables are divided into subvolumes, and a typical subvolume is shown in Fig.2. The power balance equation for subvolume *i* is

$$C_i \frac{d\theta_i}{dt} = P_i + \sum_k \frac{\theta_i - \theta_k}{R_{ik}} \qquad (1)$$

By this process the partial differential equation governing the flow of heat has been reduced to a set of (non-linear) ordinary differential equations [14]. Since the inherent time-constants within the system vary so widely, the set of equations is "stiff" [14]. Open-type integration schemes such as the Runge-Kutta methods are not suitable, as the integration time step is restricted by the shortest time constant in the system. In the case of fuses this requires time-steps of the order of tens of microseconds, giving impossibly long computing times. For stiff systems implicit methods are best, since the methods are stable regardless of the time-step chosen. Trapezoidal-rule integration [15] is one popular method, and this can easily be shown to be exactly equivalent to the Crank-Nicholson method, applied to the original PDE. The Crank-Nicholson method has previously been applied with success in fuse studies [8], but experience with the method applied to cyclic loading studies has given some problems. A flexible and robust solution method requires the integration time step to be altered dynamically during solution, but with the Crank-Nicholson method this can generate oscillations in the numerical solutions which decay only very slowly. These oscillations are well-documented [14]. Experience shows that the fully-implicit method is better for general-purpose use. Although the truncation errors are higher than the Crank-Nicholson method they can be easily controlled. The implicit method is unconditionally stable for all values of the time-step, and no oscillations occur, even for very large changes of time-step.

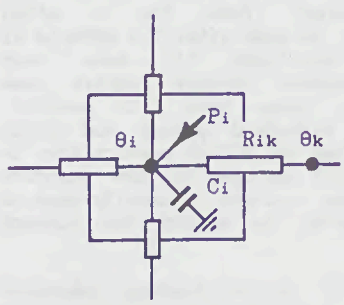

Fig. 2 Typical subvolume

For insulating materials the power generated within each subvolume is zero. For conductors the Joulean heating term P_i can be represented as $P_{0i}(1 + \alpha\theta_i)$ where P_{0i} is the power generation if the subvolume were at ambient temperature (the "cold" power). Applying now the implicit approximation to the time derivative in (1) we obtain

$$C_i \frac{\theta_i^{n+1} - \theta_i^{n}}{\Delta t} = P_{oi}^{n+1} (1 + \alpha\theta_i^{n+1}) + \sum_k \frac{\theta_i^{n+1} - \theta_k^{n+1}}{R_{ik}} \qquad (2)$$

Rearranging now so that all unknown temperature rises are on the left-hand side,

$$-\sum_k \frac{\theta_k^{n+1}}{R_{ik}} + \left[\sum_k \frac{1}{R_{ik}} + \frac{1}{R_{ci}} - \alpha P_{oi}^{n+1} \right] \theta_i^{n+1} = P_{oi}^{n+1} + \frac{\theta_i^{n}}{R_{ci}} \qquad (3)$$

where $R_{ci} = \Delta t/C_i$. If this equation is written for each subvolume we obtain the matrix equation

$$[G][\theta] = [q] \qquad (4)$$

which has to be solved at each time step to give the new subvolume temperature rises $[\theta]$. $[G]$ is the thermal admittance matrix, which can easily be assembled from a list of thermal resistances and capacitances. Each off-diagonal element G_{ij} is minus the thermal admittance connecting node i to node j. Each diagonal element G_{ii} is the sum of all admittances terminating on node i, *plus* a thermal capacitance term $1/R_{ci}$ *minus* a term αP_{oi} which allows for the temperature coefficient of resistivity. It has been assumed that the generated power at time $(n+1)\Delta t$ is a known quantity. This means that in cases where the circuit current is time-varying, the new current needs to be calculated before (4) is solved for the new temperature rises.

Software has been developed to automatically generate the subvolumes, thermal resistances and capacitances for a design such as that shown in Fig.1, in such a way that the subvolumes are concentrated in the regions where the temperature gradients are highest, e.g. around the notch zones and in the filler near to the element. This procedure reduces the truncation errors in the replacement of partial space derivatives by a network of thermal resistances. The power generation in the subvolumes is found by a separate field solution for the electrical current-density distribution, using methods based upom those which have been described previously [8], but extended for other element profiles.

3.2 Boundary conditions

For subvolumes on the interface with the surrounding ambient air, a thermal resistance needs to be included in the heat loss path from the subvolume to the ambient (reference) node, to allow for the loss of heat by natural convection and radiation. The surface heat transfer coefficient [13] is given by

$$h = C_b(\theta/D)^{0.25} + \epsilon_b\sigma \left[(\theta+T_a)^2 + T_a^2 \right] (\theta+2T_a) \qquad (5)$$

where T_a is the ambient temperature in K. A first estimate of the convection coefficient C can be obtained from standard data for the loss from horizontal or vertical cylinders of diameter D, but fine tuning of the model requires adjustment of C for each particular fuse design.

If the surface area presented to the ambient by the boundary subvolume is A the required thermal resistance in the heat loss path is $1/(hA)$. This is a non-linear function of temperature, and so $[G]$ is a (weakly) non-linear function of $[\theta]$.

3.3 Steady-state temperature distribution

If the thermal capacitance terms in (3) are omitted (or an infinitely large time step is chosen) the steady-state temperature distribution is obtained, by a single solution of (4). In this case the non-linear boundary conditions must be correctly modelled, since in the steady-state the whole of the heat generated within fuse and cables is balanced by the total heat losses from their surfaces, so accurate assessment of these losses is crucial. In order to assemble $[G]$ some estimate of the temperature vector $[\theta]$ is needed since the external thermal resistances are functions of $[\theta]$. The following iterative refinement procedure has been found suitable for solution of this problem.

1. Assume an initial set of temperature rises $[\theta]$
2. Assemble $[G]$. Solve for the temperatures $[\theta]^{new}$
3. Calculate the vector $[\Delta\theta] = [\theta]^{new} - [\theta]^{old}$
4. Adjust temperatures using $[\theta]^{adj} = [\theta]^{old} + u\,[\Delta\theta]$
5. Go to step 2 and repeat until converged

This process is not guaranteed to converge but it has been found to do so in all practical cases if a very high set of initial temperatures in used together with under-relaxation ($u < 1$). In this way convergence can be obtained in about 20 iterations, provided that the applied current is below the thermal runaway current.

3.4 Computation of minimum fusing current

It is frequently important to know the steady-state current which will just cause melting of the fuse (i.e. operation in an infinite time). This can be done by successive calls to the steady temperature calculation routine, with increasing current, until the melting point is straddled, final convergence towards the m.f.c. being achieved using the secant method [16].

3.5 Transient temperature response

When using the method described in section 3.1 to compute the transient temperature response, the main problem is that of coping with those thermal resistors which are non-linear functions of θ. One possible method may be termed the "time-lag" method [15], in which the temperatures at the previous time-step are used to calculate the resistors and hence in the assembly of $[G]$. This method is effectively an explicit method, and is only stable if small time steps are used. Experience has shown that it is not suitable for a general purpose program in which freedom to vary the time step without instablity is required, to avoid impossibly long computing times.

A much better solution is to linearise the problem by replacing the non-linear resistors by fixed resistors, the values of which are calculated at some appropriate quiescent point. In the modelling of fuse behaviour there are two important situations in which this need arises, which can be dealt with as follows.

(a) for the calculation of the time-current characteristic, and the response to a single pulse, a preliminary computation of the minimum fusing current is made and the resistors are then fixed at the values obtained at the end of this procedure. This means that when these values are used, the computed long-time end of the time-current characteristic will tend to the correct m.f.c. For shorter times the thermal resistances external to the fuse and cables play little part in determining the melting time, and so their actual values are not significant.

(b) for cyclic loading studies, the steady-state temperature rises are calculated using the r.m.s. value of the complete wave to obtain a quiescent temperature distribution, from which a set of external thermal resistors is derived. These are then fixed for the subsequent calculation of the superimposed fluctuations due to cycling.

3.6 Sparsity of [G] and solution methods

[G] is a network admittance matrix, and is symmetric, and sparse, the vast majority of entries being zero. Solution of (4) for the temperatures for models with sizes of the order of 10000 nodes cannot be achieved without exploiting the properties of [G] using sparse matrix technology [12], and ordered elimination to minimise the number of non-zero elements generated during solution. Provided that the negative term in the diagonal elements of [G] does not dominate, [G] is also a positive-definite matrix, and can be factorised into sparse upper triangular form, solution for the temperatures then being obtained by forward- and back-substitution. This factorisation process only needs to be repeated during solution if a change in time-step occurs. For transient studies with high currents it is possible that too large a time step could result in [G] not being positive definite, so if factoristion fails for this reason the time-step is progressively reduced until computation can continue.

3.7 Typical results

Fig.3 shows the computed maximum element temperature rise as a function of time for constant-current pulses of four different amplitudes applied to a fuse with a rating of approximately 120A. The temperature-time transients are plotted over a range of more than 6 decades of time, and several time zones can be identified. Also shown is a growth curve such as would be obtained from a model with a single time-constant, which has a constant initial rate-of-rise, with a slope of unity when logarithmic axes are used (curve A).

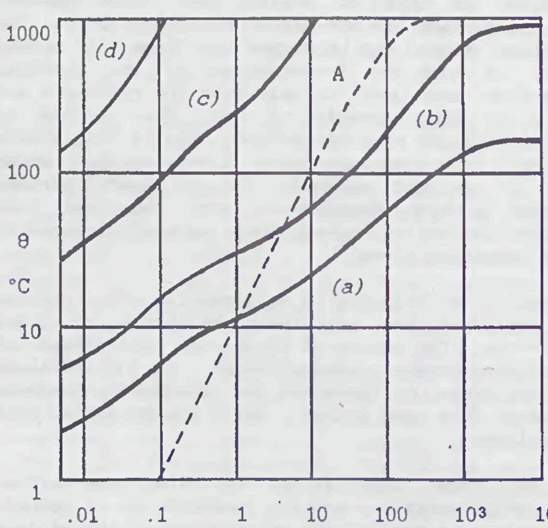

Fig. 3 Element hotspot temperature-rise

(a) at In (b) 1.55In (c) 3.5In (d) 6.8In

For times much less than 0.001 sec (not shown on Fig.3) the computed temperature-rise transients have an initial slope identical to that of curve A. This corresponds to the time zone when truly adiabatic heating of the notch zones is occurring. However, in the 0.01-0.1s time zone the computed rates-of-rise are much lower than curve A, mainly due to the effect of transient heat losses from the element notch zones to the cooler full element sections. In the 0.1-1s time zone the rates-of rise are further reduced because of the very high transient heat loss from element to the adjacent filler, the radial temperature gradients being very steep in this zone. After about 10s the radial gradients fall, the temperature-wave having penetrated by this time to the outside of the fuse body. The rate-of-rise of maximum element temperature then begins to increase noticeably, and the body temperature also begins to rise rapidly. For currents lower than the minimum fusing current, the temperature then continues to rise beyond about 1000s, but at an ever-decreasing rate, until eventual thermal equilibrium is achieved. For high currents which cause melting times less than about 0.1s the effect of the positive temperature-coefficient of resistivity can be clearly seen, as the rate-of-rise of temperature increases monotonically. Fig.3 shows that representation of fuse heating under constant current cannot be achieved by a single time-constant model even over a very limited time-zone, confirming the need for finite-difference or finite-element modelling.

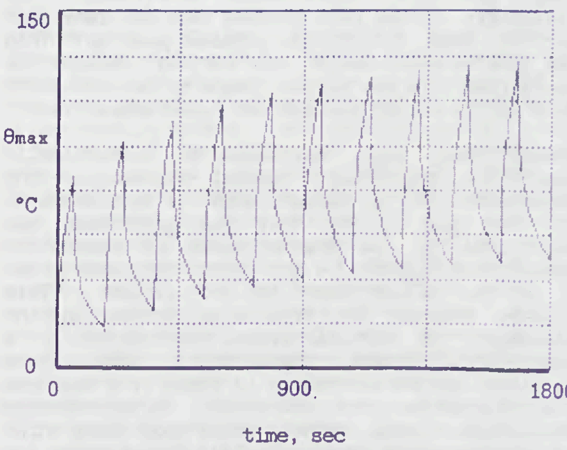

Fig.4 Cyclic loading transient (cold start)

Fig.4 shows the computed maximum element temperature for a repetitive cyclic loading condition of 150A for 1 minute followed by a 2 minute OFF period. Starting from cold, i.e. with all subvolume temperatures initially at ambient, the temperature-rise "ratchets" upwards, with the peak-to-peak excursion in temperature gradually increasing due to the effect of the temperature-coefficient. Automatic adjustment of the time-step during the solution was used to achieve a preset numerical accuracy. Eventually a quasi-stable equilibrium is reached, with constant-amplitude excursions superimposed on a quiescent temperature distribution. The time taken for the stabilisation to be reached will be of the order of thousands of seconds, and if the period of the cycle is short, say 1s ON followed by 1s OFF, the computer time required before stabilisation is excessive. The main purpose of the simulations is to compute the peak-to-peak temperature excursions after stabilisation has been reached, and the computer time needed can be reduced dramatically by using a "hot-start" condition, in which the temperature distribution in the fuse and cables is set initially to the values obtained from a steady-state solution for a steady current having the same r.m.s. value as the cyclic wave to be applied. Fig.5 shows the case of Fig.4 but with a hot-start condition. Stabilisation occurs within a few cycles, making the estimation of cyclic duty feasible even for very short duty cycles.

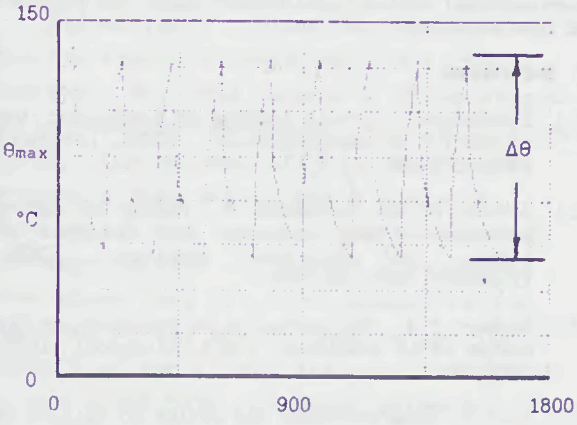

Fig.5 Cyclic loading transient (hot start)

4. Occasional overloads

Using the computational procedure described in the previous section, assessment of the effect of a single pulsed overload is straightforward. Temperature transients like those in Fig.3 are computed and terminated after the duration of the pulse. The maximum temperature reached by the fuse-element is then known, and this must be limited to some predetermined value, based upon the chemical and metallurgical nature of the material used for the element.

Rules-of-thumb based upon the used of a fixed percentage of the melting current imply that the heating curves for different currents are parallel, but Fig.3 shows that this is not so. Studies have shown that for a given fuse, with a pulse amplitude which is a fixed percentage of the melting current for the pulse duration, the maximum temperature rises produced vary by about 10% about an average value, due to the differing magnitudes of the heat losses in the various time zones previously discussed. This suggests that the rule-of-thumb for occasional overloads is quite good for a specific fuse, but there are large variations in the allowable percentage limit for different fuses, depending upon the size of the fuse and its design, so the computational model is much more convenient to use in practice.

5. Assessment of life under cyclic loading

Computation of thermal responses such as those shown in Fig.4 and Fig.5 will show immediately whether the fuse element is likely to melt under the infuence of a cyclic loading condition, and will also give the number of cycles which can be tolerated before melting occurs. This is however not very often likely to be a serious practical problem.

More important is the thermal fatigue effect. When the fuse heats up the elements expand considerably more than the fuse body, firstly because the average element temperature is much higher, and secondly because the element material usually has a very much larger expansion coefficient. A thermal stress is set up, which causes the element to deflect laterally, resulting in a thermal strain which depends upon the temperature-rise [4]. Under the action of cycling the strain fluctuates, following the peak-to-peak temperature fluctation, and thus has a time-variation which follows the shape of Fig.4.

Non-ferrous metals such as those used for fuse elements have no fatigue limit. If the peak-to-peak strain during cycling is $\Delta\epsilon$ the number of cycles to failure can be estimated using the relationship [17]

$$\Delta\epsilon = CN^{-m} \quad (6)$$

The exponent m in (6) varies in the approximate range 0.2-0.5, depending upon the metal and whether the elastic component, plastic component, or the total strain is used. In the case of cycling in fuses we cannot separate the components, so ϵ must be interpreted as the total strain (the sum of elastic and plastic deformations).

If we assume that strain is proportional to temperature-rise, the peak-to-peak strain will be given by

$$\Delta\epsilon = \beta\gamma\Delta\theta \quad (7)$$

Here γ is the expansion coefficent and β is a coefficient which relates the actual strain which occurs when the element deflects laterally to the thermal strain $\gamma\Delta\theta$. β is thus a measure of the way the mechanical design of the fuse affects its ability to withstand cycling.

Combining (6) and (7), the number of cycles to failure can be related to $\Delta\theta$. However the relationship (6) is for strain cycling at a constant temperature. The fatigue strength of silver and copper falls as the average temperature is increased [4]. Over a limited temperature range this can be allowed for by assuming that C falls with temperature according to a power-law [4], i.e. $C = \mu\theta_{av}^{-x}$. This then gives

$$\beta\gamma\Delta\theta = \mu\theta_{av}^{-x} N^{-m} \quad (8)$$

Solving for the number of cycles to failure

$$N = K\left[\Delta\theta.\theta_{av}^{x}\right]^{-1/m} \quad (9)$$

Where $K = [\mu/(\beta\gamma)]^{1/m}$ and is a constant for a given fuse design. The higher the value of K the better the fuse's ability to withstand cycling.

The relationship (9) has been tested using the results of cyclic loading endurance tests on a number of semiconductor fuses with silver elements, of various designs. For each test the number of cycles to failure N is known, and the temperature terms $\Delta\theta$ and θ_{av} were found for the given loading condition using the computer model. To achieve higher accuracy in the calculations, the following method was used. First the time-current characteristic for each fuse was obtained using the computer model. The cyclic performance was then computed using ON-state currents which were the same percentage of the *computed* time-current characteristic as was used in the tests, relative to the actual characteristic. This ensured good accuracy in the temperature values.

Application of multiple regression analysis to the results showed that :

(a) there was, as expected, a strong dependence of N upon $\Delta\theta$. The best-fit value for 1/m was 3.85, giving m=0.26, which is within the expected range for the fatigue law of equation (6).

(b) there was a much weaker dependence of N upon θ_{av}. The best-fit value for x/m was 0.658, giving x=0.171, which is a measure of the fall in fatigue strength with temperature.

(c) the values of K showed a consistent pattern which gave a direct measure of the ability of a given design to withstand cyclic loading.

Fig.6 shows how the life curves for two different fuses conform to equation (9). The test results were obtained using a variety of different duty cycles, but use of the computer model to convert these test conditions to values of $\Delta\theta$ and θ_{av} bring all the results together into a band. There is a scatter in life of about one power of ten, which is typical of fatigue failures, but the general trend is in agreement with the fatigue model of equation (9).

This then gives a quick method of assessing expected life of a fuse under given cyclic loading conditions, or, conversely, of selecting a fuselink for an application to ensure that an acceptable life is achieved.

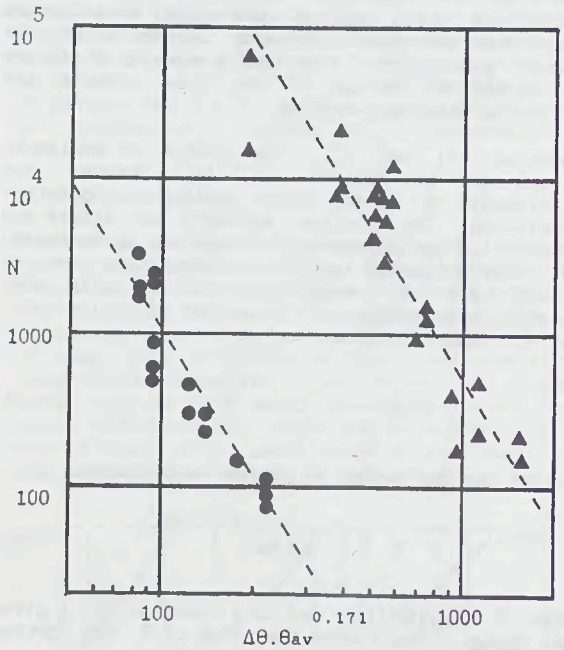

Fig. 6 Typical endurance characteristics

6. Conclusions

Simulation of the thermal behaviour of fuses for the protection of semiconductors under pulsed and/or cyclic loading conditions requires a comprehensive finite-difference model with a number of nodes of the order of 10,000 if accurate results are to be obtained from the simulation. The development and testing of such a simulation method has been undertaken, based upon the use of the RC-network analogue, and using sparse matrix methods with ordered elimination to obtain rapid solutions.

From drawings of a fuse design and knowledge of the materials used in its construction and their properties, predictions of the element temperature distributions can be obtained, and used to assess the fuse's suitability for a given application. A simple fatigue model has been developed which enables selection of a fuse to withstand a given number of cycles.

For straightforward cases, applications engineers will no doubt continue to use the rules-of-thumb for fuse selection when these are known by experience to give good results, but in those special cases which arise so frequently in protection problems involving power semiconductors, the availability of the new computational method considerably eases the problems of fuse selection.

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