Board-Mounted Electronic Component Transient Thermal Behavior: CFD Prediction Versus Measurement

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Abstract

Numerical predictive accuracy is investigated for transient component heat transfer using a Computational Fluid Dynamics (CFD) code dedicated to the thermal analysis of electronic equipment. The test cases are based on a single Printed Circuit Board (PCB)-mounted, 160-lead PQFP component, analyzed in still-air, and both 1 and 2.25 m/s forced airflows. Three types of transient operating conditions are considered, namely (i) component dynamic power dissipation in fixed ambient conditions, (ii) passive component operation in dynamic ambient conditions, and (iii) combined component dynamic power dissipation in varying ambient conditions. Benchmark criteria are based on component junction temperature and component-PCB surface temperature, measured using thermal test dies and infrared thermography respectively.

Using both nominal component/PCB geometry dimensions and material properties, component junction temperature is found to be accurately predicted for component dynamic power dissipation, in both fixed and varying ambient air temperature conditions. The results suggest that CFD analysis could play an important role in providing critical boundary conditions for component electrical and thermo-mechanical behavior analyses. However, caution is stressed on the use of heat transfer predictions for multi-component board applications.

Key Words

CFD, component modeling, benchmark, electronics cooling, transient heat transfer, reliability.

Nomenclature

T temperature, °C

Subscripts

a ambient
j component junction

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1. Introduction

The continuous increases of product functionality and miniaturization have inadvertently resulted in rising die heat fluxes [1], which, if not efficiently removed from the device, may induce elevated operating temperature. While it has been shown that die circuit electrical performance can be highly sensitive to operating temperature [2], many integrated circuit packaging failure mechanisms have been found to be dependent upon spatial temperature gradients, temperature cycle magnitude and rate of temperature change, rather than absolute steady-state temperature [3]. With ever-reducing product development cycle times preventing both extensive prototyping and the acquisition of field experience in the use of new packaging technologies, increasing reliance is being placed on numerical predictive techniques, such as Computational Fluid Dynamics (CFD), to predict these variables. These predictions then form critical boundary conditions for electrical and thermo-mechanical performance analyses, component life and reliability calculations.

While many electronic parts are subjected to transient operating conditions in the course of their life, due to dynamic power operation or varying ambient conditions, Parry et al. [4] note that over 90% of numerically-based thermal analyses performed on electronic equipment in recent years have been steady-state. This is essentially attributed to previous reliability prediction methods, such as MIL-HDBK-217F, focusing on steady-state temperature, as well as design for continuous operation and prohibitive computational requirements for transient analysis. Previously reported numerical studies on transient component heat transfer have been confined to the analysis of conduction-cooled high-power modules, such as IGBT devices [5-8]. The cooling configuration permitted these analyses to be confined to the modeling of conduction, with either a fixed temperature boundary condition or effective heat transfer coefficient prescribed at the domain boundary. Though justified in such applications, this modeling approach would not be appropriate for the majority of air-cooled, board-mounted components, from which heat transfer is highly conjugate.

The need for accurate transient analysis is now also motivated by Physics-of-Failure (PoF) reliability prediction methods [9-11], which require the knowledge of component transient operating temperature for assessing electrical and thermo-mechanical performance. Despite increases in computational power, a fully coupled thermal and mechanical analysis is not yet feasible [12] and instead, sequential approaches are employed. With short development cycle times prohibiting separate detailed thermal analysis, thermo-mechanical analysis is generally constrained to approximating convective heat transfer at the solid boundary using prescribed boundary conditions, derived from semi-empirical correlations, or to applying fixed temperature boundary conditions within the solid domain. The potential shortcomings of such approximations are highlighted by Wakil and Ho [13], who found that isothermal loading may lead to significant modeling errors for the prediction of the strain distribution within a heat dissipating PQFP component. They concluded by stressing the need for accurate modeling of the temperature distribution within the component body. The application of Rayleigh-Nusselt correlations has been shown to permit accurate prediction of single-component Printed Circuit Board (PCB) heat transfer in free convection [14,15], but cannot account for the impact of neighboring component thermal interaction on operating temperature in multi-component PCB applications [16]. For forced convection heat transfer, little agreement exists
between the various Nusselt-Reynolds correlations that have been developed [17] due to the difficulty of defining a dominant characteristic dimension for component shape and PCB configuration [18,19]. Consequently, temperature predictions are highly sensitive to the correlation used. Realistically therefore, board-mounted component conjugate heat transfer can only be predicted using CFD methods.

Apart from the prediction of transient component operating temperature, a potential application area of CFD analysis could be the design of both optimum assembly processes and reliability testing conditions. The thermal stresses induced during assembly processes, such as surface-mount soldering, have been well documented [20]. On the other hand, PoF approach-based reliability prediction methods rely on the accurate determination of testing parameters, which must accelerate the same failure mechanisms as those taking place in the application environment. Warner et al. [12] point out that it is difficult to include, for example, the temperature difference within the package and board in an experimental accelerated environment, and that to accelerate this temperature difference requires the knowledge of the application environment. In such instances, and on the premise that sufficient predictive accuracy can be obtained, CFD analysis could provide the necessary boundary conditions.

This study attempts to address some of the weaknesses highlighted by investigating the prediction of transient board-mounted component conjugate heat transfer, using a CFD code dedicated to the thermal analysis of electronic equipment. The applicability of CFD analysis to predict steady-state, single-component PCB heat transfer in free and forced convection has been well established [18,19,21-24]. For such applications, component junction temperature prediction accuracy was found to be within ±3°C or ±5% of measurement, which meets the accuracy requirement for using temperature predictions as boundary conditions in product performance and reliability analyses [25]. However, the forced convection studies [18,19,22,24] also showed that predictive accuracy decayed up to ±10°C or ±20% on multi-component PCBs. Prediction errors were associated with both the more complex aerodynamic conditions over the board and component thermal interaction being incorrectly predicted. These findings therefore point towards the necessity of extending such assessment to the analysis of transient component heat transfer.

As a first step, in this study CFD predictive accuracy is assessed for single-component PCB heat transfer. The test cases are based on a single-board mounted 160-lead PQFP, analyzed in both free convection, and forced airflows generated by a wind tunnel. Three types of transient operating conditions are considered, namely (i) component dynamic power dissipation in fixed ambient conditions, (ii) passive component operation in dynamic ambient conditions, and (iii) combined component dynamic power dissipation in varying ambient conditions. Benchmark criteria are based on component junction and component-PCB surface temperatures, measured using thermal test dies and infrared thermography respectively. These measurements were taken with the test vehicle mounted in a still-air enclosure and wind tunnel for free and forced convection analysis respectively. Component and PCB numerical modeling is based on nominal package dimensions and material properties. Before assessing predictive accuracy for transient heat transfer, the component-PCB numerical model is validated for steady-state heat transfer. Any significant decay in component junction predictive accuracy for test cases (i)
relative to the levels obtained for steady-state transfer would therefore be associated with the modeling of the component-PCB thermal capacitance.

2. Experimentation

The component junction and component-PCB surface temperature measurements used to assess predictive accuracy were undertaken by Davies et al. [26] for the free convection test cases, and Lohan and Davies [27] for the forced convection analyses. The measured free convection component transient response analyzed in this study was not presented by Davies et al., who only report the corresponding steady-state thermal resistance.

The test board, shown in Figure 1, was a 1.6 mm thick FR-4 design of size 116 mm x 78 mm, with one-ounce copper tracking on both sides, covering approximately 20% of the board surface area. The component was a thermally enhanced PQFP package, Figure 2, having an embedded 18 mm square heat slug. This device contained a 7.5 mm square thermal test die conforming to the SEMI standard G32-94 [28] for junction temperature measurement, which was calibrated to an accuracy of ±0.4°C. For infrared surface temperature measurement, the component and PCB surfaces were sprayed with a uniform layer of matt black paint having an emissivity of 0.96. These measurements were made using an AGEMA infra-red Thermovision 880 system operating in the 8 to 12 um spectral range, with a specified accuracy of ±2°C.

Free and forced convection characterizations were performed in a still-air enclosure and variable speed heated wind tunnel respectively. The still-air enclosure was a square box of volume 0.02832 m3, conforming to the SEMI standard G38-87 [29]. Forced convection characterization was performed with the PCB assembly vertically mounted at the center of the wind tunnel test section, which had cross-sectional dimensions of 125 x 125 mm. Air temperature control to an accuracy of 2°C was achieved using a programmable controller and feedback thermocouple located beside the test assembly in the test section. Component steady-state junction temperature measurements were carried out in accordance with the SEMI standard G38-87 [29]. Component transient junction temperature was recorded using a standard high-speed data acquisition system.

The transient operating conditions for the respective test cases are described in Tables 1 to 3. For all component powered-on cases, the device dissipated 3 Watts.

3. Numerical Models

Numerical analysis was undertaken using Flotherm, Version 3.1, a CFD code widely used within the industry for the analysis of electronics cooling. The computational method is given in [30].

The constraints typically imposed on thermal design processes [18,24] motivated the pragmatic approach adopted for component and PCB modeling. All dimensions and constituent material thermal properties correspond to nominal vendor specifications listed in Table 4, with the exception of FR-4 thermal conductivity. For this parameter, the anisotropic value measured by Graebner & Azar [31] was applied, instead of the vendor isotropic specification of 0.3 W/m.K, which is only representative of the through-plane...
Figure 1. Test Printed Circuit Board

Figure 2. 160-lead PQFP component geometry
Table 1. Component dynamic power dissipation in fixed ambient conditions

<table>
<thead>
<tr>
<th>Test case</th>
<th>Convecting environment</th>
<th>Duration of power-on from start of test (s)</th>
</tr>
</thead>
<tbody>
<tr>
<td>A</td>
<td>Free</td>
<td>1000</td>
</tr>
<tr>
<td>B</td>
<td>Forced, 1 m/s</td>
<td>247.5</td>
</tr>
</tbody>
</table>

Note: Ambient air temperature = 20ºC. Component power dissipation = 3W.

Table 2. Passive component operation in dynamic ambient air temperature conditions.

<table>
<thead>
<tr>
<th>Test case</th>
<th>Free-stream air velocity (m/s)</th>
<th>Ramp rate (ºC/min)</th>
<th>Dwell time (s)</th>
</tr>
</thead>
<tbody>
<tr>
<td>C</td>
<td>1.0</td>
<td>15</td>
<td>300</td>
</tr>
<tr>
<td>D</td>
<td>1.0</td>
<td>25</td>
<td>300</td>
</tr>
<tr>
<td>E</td>
<td>2.25</td>
<td>5</td>
<td>60</td>
</tr>
<tr>
<td>F</td>
<td>2.25</td>
<td>15</td>
<td>60</td>
</tr>
</tbody>
</table>

Note: Ramp rate refers to rate of change of ambient air temperature from 30ºC to 110ºC. Dwell time refers to duration at maximum ambient air temperature.
Table 3. Combined component dynamic power dissipation in varying ambient air temperature conditions.

<table>
<thead>
<tr>
<th>Test case</th>
<th>Convecting environment</th>
<th>Ramp rate (°C/min)</th>
<th>Dwell time (s)</th>
<th>Duration of power-on from start of test (s)</th>
</tr>
</thead>
<tbody>
<tr>
<td>G</td>
<td>Forced, 1 m/s</td>
<td>15</td>
<td>60</td>
<td>180</td>
</tr>
<tr>
<td>H</td>
<td>Forced, 2.25 m/s</td>
<td>15</td>
<td>60</td>
<td>180</td>
</tr>
</tbody>
</table>

Note: Ramp rate refers to rate of change of ambient air temperature from 30°C to 110°C. Dwell time refers to duration at maximum ambient air temperature. Component power dissipation = 3W.

conductivity. However, modeling FR4 conductivity with this isotropic value had a negligible effect on junction temperature due to copper tracking dominating PCB heat spread.

The component and PCB modeling methodologies are based on Rosten's et al. approach [32], with minor alterations described in Eveloy et al. [22]. To eliminate the computational constraints associated with explicitly modeling the lead frame and external leads geometries, both were modeled using a so-called 'compact model' approach. This permits these geometries to be modeled as single cubical blocks having effective thermal conductivity, density and specific heat capacity values, which are calculated based on the volumetric ratios of the constituent solid materials. The robustness of this modeling methodology was demonstrated for steady-state heat transfer [23], with the device mounted on a different PCB than used in this study. Component junction temperature prediction accuracy was found to be within -3°C (5%) of measurement when account was made of experimental error. PCB heat spread was also shown to be correctly captured based on measured infrared surface temperature profiles. This validation procedure was repeated for the present PCB, Figure 1, having both different dimensions and a higher copper tracking density in the vicinity of the component.

The detailed component model is represented in Figure 3, with the free and forced convection numerical models shown in Figure 4. Computational domain dimensions and grid details are given in Table 5 for both models.

For the free convection model, the computational domain was confined to the fluid domain in the vicinity of the PCB to permit the computational grid to be effectively used to focus on the resolution of the component-PCB thermofluids. Free-air boundary conditions were applied at the computational domain boundaries, positioned at a sufficient distance from the PCB assembly so that no significant unintentional elliptical effects were introduced.
Table 4. Nominal material thermal property values for component and PCB constituent elements

<table>
<thead>
<tr>
<th>Element</th>
<th>Thermal conductivity (W/m.K)</th>
<th>Density (kg/m³)</th>
<th>Specific heat capacity (J/kg.K)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Encapsulant</td>
<td>0.63</td>
<td>1820</td>
<td>882</td>
</tr>
<tr>
<td>Die</td>
<td>117.5 - 0.42 (T-100)</td>
<td>2330</td>
<td>712</td>
</tr>
<tr>
<td>Die attach</td>
<td>1.9</td>
<td>-</td>
<td>-</td>
</tr>
<tr>
<td>Leadframe</td>
<td>301.5</td>
<td>8900</td>
<td>385</td>
</tr>
<tr>
<td>Heat slug</td>
<td>398.0</td>
<td>8940</td>
<td>385</td>
</tr>
<tr>
<td>Leadframe insulation</td>
<td>0.2</td>
<td>-</td>
<td>-</td>
</tr>
<tr>
<td>PCB substrate</td>
<td>k&lt;sub&gt;ip&lt;/sub&gt; = 0.81, k&lt;sub&gt;tp&lt;/sub&gt; = 0.29</td>
<td>1920</td>
<td>1300</td>
</tr>
<tr>
<td>PCB copper tracking</td>
<td>398</td>
<td>8933</td>
<td>383</td>
</tr>
</tbody>
</table>

Note: T = temperature in °C. k<sub>ip</sub> and k<sub>tp</sub> are in-plane and through-plane thermal conductivities values respectively [31].

These artificial boundary conditions were permissible as the enclosure roof did not adversely impact on the buoyant thermal plume emanating from the PCB assembly, and as there was negligible thermal stratification in the vicinity of the PCB assembly. These free-air boundaries fixed the relative pressure to zero with any incoming air entering at the prescribed ambient temperature of 20°C. Their location was approximately 62 mm from the PCB component- and non-component sides, 100 mm from both PCB vertical edges, flush with the bottom edge of the PCB, and 84 mm above the PCB top edge.

For the forced convection models, a uniform free-stream velocity inlet boundary condition was applied 57.5 mm upstream of the PCB leading edge, and an outlet vent was positioned 100 mm downstream of the PCB trailing edge. The domain was extended to the wind tunnel test section walls in both the span-wise and transverse directions. The test section surfaces were modeled using the code default friction setting for smooth surfaces. As discrepancies existed between the programmed and measured test section free-stream air temperature cycles, resulting from the thermal inertia of the heater, the experimentally recorded time-temperature profiles were modeled using a numerical heater located at the test section inlet, Figure 4(b).

Based on infrared measurements of PCB surface temperature, which revealed negligible thermal interaction between the PCB and its mechanical support, this fixture was modeled as non-conducting.

For steady-state free convection heat transfer, the board Grashof number, calculated from the thermographic measurements, was on order 10^6. The calculated board Reynolds number

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Figure 3. 160-lead PQFP component numerical model

Table 5. Computational domain size and spatial grid discretization detail for the numerical models, Figure 4.

<table>
<thead>
<tr>
<th></th>
<th>Free convection model</th>
<th>Forced convection model</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>x</td>
<td>y</td>
</tr>
<tr>
<td>Domain size (mm)</td>
<td>193</td>
<td>200</td>
</tr>
<tr>
<td>Computational grid</td>
<td>124</td>
<td>93</td>
</tr>
</tbody>
</table>

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Figure 4. Component-PCB numerical models
for steady-state heat transfer at 2.25 m/s was on order $10^4$. Consequently, the fluid domain was solved as laminar for all test cases, with variable fluid property treatment applied. For the transient test cases, both heat transfer and fluid flow were solved as unsteady.

Radiative heat transfer was modeled from the component top and bottom surfaces, PCB FR-4 substrate and copper tracking surfaces.

For both the free and forced convection models, a non-uniform spatial grid was applied having highest density both within the component body to resolve conductive heat spread, and in the near-wall regions, to resolve the high velocity and temperature gradients within the hydrodynamic and temperature boundary layers respectively, and thus their near-wall effects on both surface friction and heat transfer. Extensive sensitivity checks were performed to obtain grid-independent solutions. The spatial grid discretization details are given in Table 5 for both the free and forced convection models.

For the transient analyses, a non-uniform temporal grid was applied having highest density in the time intervals where high rates of temperature change were experimentally recorded on the test assembly. The solutions obtained were verified to be temporal grid independent. This grid was constructed using time steps ranging from 3 ms to 5 s.

Solution convergence was defined when the residual error sum for each variable was reduced to the termination error level, which was set to the code default settings.

Computation was performed using a DELL Precision 420 workstation with dual 1 GHz Pentium III processor and 1024 MB RAM, operating on Windows 2000 Professional. Computational time varied from on order 4 hours for the steady-state free convection case, to 24 hours for the transient test cases.

4. Results and Discussion

Before analyzing the transient test cases, predictive accuracy is assessed for steady-state heat transfer, whereby the variable of thermal capacitance is eliminated.

Steady-state heat transfer

Component junction temperature prediction accuracy is presented both as an absolute temperature error ($^\circ$C), and percentage value in Table 6. In both free convection and 1 m/s airflow, prediction accuracy is within -2.3$^\circ$C or 4% of measurement, but decreases to -3.8$^\circ$C or 9% at 2.25 m/s. When account is made of experimental error, such accuracy would therefore be sufficient for the temperature predictions to be used in steady-state performance and reliability analyses [25].

PCB heat spread is also correctly captured by the model, as shown in Figure 5 for free convection, and Figure 6 for 2.25 m/s airflow. In both cases, both the magnitude and shape of the measured and predicted component-PCB surface temperature profiles are in good agreement in both the span-wise and stream-wise directions. The discrepancies between predictions and measurements over the component leads are primarily attributed to experimental error, resulting from the spatial temperature resolution of the infrared measurement system, which would result in temperature averaging over the package leads and adjacent component top and PCB surfaces. This factor may also explain the discrepancy between the measured and predicted PCB temperatures in the vicinity of the component in the span-wise direction in free convection, Figure 5(a), on the profile left-hand side. For this case, large temperature gradients exist in this region of the board as the copper tracks only extend by 5 mm from the component body, Figure 1. The magnitude
Table 6. Comparison of measured and predicted component steady-state junction temperatures

<table>
<thead>
<tr>
<th>Convecting environment</th>
<th>Measured (ºC)</th>
<th>Prediction discrepancy (ºC)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Free</td>
<td>83.6</td>
<td>-2.3 (3.6%)</td>
</tr>
<tr>
<td>Forced, 1 m/s</td>
<td>70.3</td>
<td>-2.1 (4.2%)</td>
</tr>
<tr>
<td>Forced, 2.25 m/s</td>
<td>61.8</td>
<td>-3.8 (9.1%)</td>
</tr>
</tbody>
</table>

Note: Measurement accuracy, ±0.4ºC. Percentage prediction error in parenthesis ( ) is calculated based on the measured component junction temperature rise above ambient air temperature. Component power dissipation = 3W. Ambient air temperature = 20ºC.

of the underprediction of component surface temperature at 2.25 m/s, Figure 6, is in line with that for junction temperature in Table 6. This discrepancy is attributed to a slight overprediction of PCB heat spread, as the predicted board surface temperature is slightly higher than the corresponding measurement, Figure 6. This suggests that the component junction temperature prediction error could be attributable to the modeling of the PCB copper tracking. However, computational constraints prohibit the explicit modeling of its geometric detail.

Based on these analyses, any significant decay in component junction predictive accuracy for the component dynamic power dissipation test cases in fixed ambient conditions, Table 1, would be associated with the modeling of the component-PCB assembly thermal capacitance.

Transient heat transfer

Component dynamic power dissipation in fixed ambient conditions, Table 1. The measured and predicted component transient thermal responses are compared in Figures 7 and 8 in free convection and 1 m/s airflow respectively. For both convecting environments, predicted component junction temperatures are on order within -2ºC of measurement, indicating that the component-PCB junction-to-ambient thermal impedance is correctly modeled. It can therefore be concluded that potential uncertainties in material thermo-physical properties do not impact on predictive accuracy in this instance.

Passive component operation in dynamic ambient air temperature conditions, Table 2. The measured and predicted component transient thermal responses are compared in
Figure 5. Comparison of measured and predicted component-PCB surface temperature profiles for steady-state free convection heat transfer.
Figure 6. Comparison of measured and predicted component-PCB surface temperature profiles for steady-state heat transfer in a 2.25 m/s airflow
Figures 9 and 10 for 1 m/s airflow, and Figures 11 and 12 for 2.25 m/s.

1 m/s air temperature cycles, Tests C and D. During the heating phase of the test assembly up to approximately 400 s, measured and predicted component junction temperatures are in excellent agreement, indicating that the system thermal impedance is correctly modeled. However, measurements and predictions diverge beyond this point till the end of the imposed dwell, where the discrepancy stabilizes at a maximum value of 4.6°C and 3.9°C for tests C and D respectively. Despite this error, the shape of the predicted transient response during the cooling phase is in excellent agreement with measurement, as found during the heating phase up to 400 s. This trend confirms that the system thermal capacitance is correctly modeled, with the discrepancy observed during the dwell period being therefore related to the prediction of the steady-state thermal resistance. Such a discrepancy was not found for the component powered-on cases, Tests A and B, and is primarily attributed to experimental error. Though air density variation was numerically accounted for, Lohan and Davies [27] measured a 6% increase in airflow velocity within the test section for the temperature range under analysis. This occurred as the wind tunnel motor operated at a fixed speed, with airflow velocity consequently increasing due to lower pressure drop. This velocity variation, which could not be modeled, would therefore result in the slight overprediction of the component-PCB thermal resistance during the dwell period.

2.25 m/s air temperature cycles, Tests E and F. The same trends are observed at 2.25 m/s in Figures 11 and 12 as for the 1 m/s analyses. However, minor discrepancies can be detected between measurements and predictions during the heating phase of the test assembly, which were not observed at 1 m/s. These discrepancies could be attributable to the lower accuracy obtained for steady-state heat transfer at 2.25 m/s, Table 6, which impacts on the prediction of the test assembly thermal time constant, hence heating rate.

Combined component dynamic power dissipation in varying ambient air temperature conditions, Table 3. Measured and predicted component junction temperatures are compared in Figures 13 and 14 for 1 m/s and 2.25 free-stream air velocities respectively. Overall, the shape of the predicted component transient thermal response is in good agreement with measurement, both during the powered-on heating phase, and cooling phase beyond the imposed dwell period. Good accuracy is also obtained between the end of the power dissipation pulse and the end of the dwell period, during which a complex redistribution of the heat transfer paths occurs. Overall, prediction discrepancies reflect those observed for the active and passive component operation test cases previously analyzed, that is:

(i) The junction-to-ambient thermal impedance is underestimated during component dynamic operation, as for Test B.

(ii) The junction-to-ambient thermal impedance is overestimated from the end of the imposed dwell period onwards, as for Tests C – F, which was primarily attributed to experimental error.

The results of these analyses combined show that based on both nominal component/PCB geometry dimensions and material thermo-physical properties, single component-PCB transient heat transfer can be predicted with good accuracy, in both free and forced convection. In this instance therefore, confidence could be gained in applying CFD
Figure 7. Comparison of measured and predicted transient component junction temperature rise for a continuous power dissipation of 3W in a quiescent air at 20°C, Test A

Figure 8. Comparison of measured and predicted transient component junction temperature rise for both continuous and pulsed 3W component power dissipation in a 1 m/s airflow at 20°C, Test B
Figure 9. Comparison of measured and predicted passive component junction temperature in dynamic ambient air temperature conditions (15°C/min ramp, 300s dwell time), in a 1 m/s airflow, Test C.

Figure 10. Comparison of measured and predicted passive component junction temperature in dynamic ambient air temperature conditions (25°C/min ramp, 300s dwell time), in a 1 m/s airflow, Test D.
Figure 11. Comparison of measured and predicted passive component junction temperature in dynamic ambient air temperature conditions (5°C/min ramp, 60s dwell time), in a 2.25 m/s airflow, Test E

Figure 12. Comparison of measured and predicted passive component junction temperature in dynamic ambient air temperature conditions (15°C/min ramp, 60s dwell time), in a 2.25 m/s airflow, Test F
**Figure 13.** Comparison of measured and predicted transient component junction temperature rise for a pulsed 3W component power dissipation in dynamic ambient air temperature conditions (15°C/min ramp, 60s dwell time), in a 1 m/s airflow, Test G

**Figure 14.** Comparison of measured and predicted transient component junction temperature rise for a pulsed 3W component power dissipation in dynamic ambient air temperature conditions (15°C/min ramp, 60s dwell time), in a 2.25 m/s airflow, Test H

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analysis to generate temperature boundary conditions for use in product electrical and thermo-mechanical performance analyses. This approach would permit the generation of more realistic temperature boundary conditions, as opposed to those obtained using prescribed convective heat transfer boundary conditions derived from semi-empirical analysis. The passive component operation cases in varying ambient conditions indicate that CFD analysis could also be used to optimize assembly processes, where the aim is to minimize thermal gradients, hence stresses. Conversely, CFD analysis could serve to determine HALT (Highly Accelerated Life Testing) parameters. Such variables may be difficult, if not impossible to measure experimentally.

The results also suggest that the component modeling methodology employed would be sufficiently robust to be used for the derivation of dynamic component Compact Thermal Models (CTMs) [33,34]. This study can therefore be seen as a contribution to this area. The assessment of detailed modeling methodologies for transient component heat transfer has recently been investigated by Schweitzer and Pape [35] using dual cold plate boundary conditions. In contrast to the approach employed in the present study, however, the lead frame geometry was explicitly modeled.

Nevertheless, as predictive accuracy for steady-state component heat transfer has been found to significantly decay from single- to multi-component board applications [18,19,22,24], this issue is anticipated to have a comparable impact for the analysis of transient operating conditions. In such instances therefore, caution should be stressed on applying CFD-generated temperature predictions to performance and reliability analyses.

5. Conclusions

Conjugate transient heat transfer from a single board-mounted electronic component was numerically modeled using a CFD code dedicated to the thermal analysis of electronic equipment.

Using both nominal component/PCB geometry dimensions and material thermo-physical properties, component junction temperature prediction accuracy was found to be accurately predicted for component dynamic power dissipation, in both fixed and varying ambient air temperature conditions.

The results suggest that CFD analysis could play an important role in providing critical boundary conditions for component electrical performance and thermo-mechanical behavior analyses.

However, based on previous studies, caution is stressed on the use of CFD-generated temperature predictions for multi-component board applications.

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References


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