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S. R. Annapragada
UTRC

T. Salamon
Alcatel-Lucent

P. Kolodner
Alcatel-Lucent

M. Hodes
Tufts University

S V. Garimella
Purdue University, sureshg@purdue.edu

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DETERMINATION OF ELECTRICAL CONTACT RESISTIVITY IN THERMOELECTRIC MODULES (TEMS) FROM MODULE-LEVEL MEASUREMENTS

S. Ravi Annapragada, Purdue University*

Todd Salamon, Alcatel-Lucent

Paul Kolodner, Alcatel-Lucent

Marc Hodes, Tufts University

Suresh V. Garimella, Purdue University

*Phone: (765) 494-5646

School of Mechanical Engineering and Birck Nanotechnology Center

Purdue University, 585 Purdue Mall

West Lafayette, Indiana, USA, 47907-2088

Email: asravi@purdue.edu

ABSTRACT

An experimental apparatus was developed to characterize the performance of a thermoelectric module (TEM) and heat sink assembly when the TEM was operated in refrigeration mode. A numerical model was developed to simulate the experiments. Bulk and interfacial Ohmic heating, the Peltier effect, Thomson effect and temperature-dependent bulk material properties, i.e., Seebeck coefficient and electrical conductivity were considered. A novel, self-consistent characterization methodology was developed to obtain the electrical contact resistivity at the interconnects in a TEM from the numerical simulations and the experiments. The electrical contact resistivity of the module tested was determined to be approximately $1.0 \times 10^{-9} \Omega \text{m}^2$. The predictions are consistent with electrical contact resistivity obtained based on the performance specifications (ΔT_{max}) of the TEM.

KEY WORDS: thermoelectric module, electrical contact resistance, cooling.

NOMENCLATURE

A	area, m ²	Subscripts	
e	unit vector in direction of current flow	amb	ambient
G	geometric factor (area of cross-section/length of pellet), m	c	cold side
		ec	electrical contact
H	height	h	hot side
I	current, A	$load$	patch heater
k	thermal conductivity of a thermocouple, Wm ⁻¹ K ⁻¹	max	maximum
K	thermal conductance of a thermocouple, WK ⁻¹	$ohmic$	I ² R losses due to Ohmic resistance
N	number of thermocouples	$para$	parasitic
q	heat load, W	$parallel$	parallel network equivalent
R	Ohmic resistance of a thermocouple, Ω	$peltier$	Peltier effect
T	temperature, K	P	dimensions related to thermoelectric pellet
V	voltage, V	$thomson$	Thomson effect
W	TEM power, W	TEM	dimensions related to overall TEM
Greek symbols		Superscripts	
α	Seebeck coefficient, VK ⁻¹	T	thermal
ρ	electrical resistivity, Ω m (bulk) or Ω m ² (contact)		

I. INTRODUCTION

Thermoelectric modules (TEMs) are solid state devices that exploit thermoelectric effects in order to cool electronic components or generate electric power [1,2]. Cooling (or heating) is accomplished by passing current through the thermoelectric materials in order to generate temperature differences. Moreover, precision temperature control [3], a major application of TEMs, is accomplished by controlling the direction of the current flowing through a TEM to operate it in either cooling or heating modes and its magnitude to regulate the rate of cooling or heating. Since this is done via solid-state electronics, rapid responses to, for example, changes in the heat dissipated by a device mounted to a TEM [4] are achieved. This is highly desirable for photonics components operating under varying ambient conditions and/or heat loads. Hence, precision temperature control of photonics components using TEMs is commonplace.

A schematic of a TEM operating in refrigeration mode is shown in Fig. 1. It consists of an array of n- and p- type semiconductor pellets connected electrically in series and thermally in parallel between ceramic substrates. Each adjacent pair of n- and p-type pellets is referred to as a thermocouple and there are N thermocouples in a TEM. When a TEM is operated in refrigeration mode, the objective is to cool the component mounted to it below the local ambient temperature. Hence, the side of the TEM to which the component is attached is termed the “cold side.” The other side of the TEM, i.e., the “hot side,” is attached to a heat sink and dissipates the heat generated by the component and the TEM.

Fig. 1 Schematic illustration of a TEM in refrigeration mode.

Although thermoelectric effects are thermodynamically reversible, accompanying bulk and interfacial Ohmic heating and heat conduction through finite temperature gradients are irreversible. The electrical resistivity and thermal conductivity of standard thermoelectric materials are known. However, comparatively little data exist on measured electrical contact

resistivities at the interconnects in a TEM. Impurities in the material, variation of the semiconductor crystal size and defects at the semiconductor-conductor interface cause resistance to the flow of electricity through it [5]. These effects are all summed up under electrical contact resistance.

When bismuth telluride (Bi_2Te_3) pellets are soldered to their interconnects in conventional TEMs used in refrigeration, the electrical contact resistivity is typically between 10^{-9} and $10^{-8} \Omega\text{m}^2$ [5]. Böttner et al. [6] reported electrical contact resistivities of about $10^{-10} \Omega\text{m}^2$ for soldered interconnects in microstructured Bi_2Te_3 TEMs fabricated using thin-film technology. Moreover, da Silva and Kaviani [5] concluded that the electrical contact resistivity of Bi_2Te_3 -metal interfaces may be reduced to $2 \times 10^{-11} \Omega\text{m}^2$ or lower. Recently, Peltier cooling at heavily-doped Si-metal interfaces has been exploited for thermal management of localized high heat flux spots on microprocessors (see, e.g., Wang and Bar-Cohen [7]) and the electrical contact resistivity at such an interface can in theory be as low as approximately $10^{-13} \Omega\text{m}^2$ [8].

Recently, superlattice-based thin-film TEMs [9] have been integrated into electronics packages to cater to on-demand and site-specific cooling needs. The rate of interfacial Ohmic heating in such thin-film TEMs supersedes the bulk Ohmic heating in the pellets due to their small height [5,10]. The predicted maximum cooling achieved by the thin-film TEMs, with and without the interfacial resistances included, were 14.9°C and 25°C , respectively [9] for a heat load of $1,300 \text{ Wcm}^{-2}$. Krishnan et al. [11] showed thin-film TEMs to be a viable technology to cool high heat fluxes in electronic equipment. The interfacial Ohmic heating was shown to drastically affect the performance of these TEMs, especially at micron sizes. The performance of the TEM with $100 \mu\text{m}$ tall pellets and a contact resistivity of $10^{-9} \Omega\text{m}^2$ dropped to 50% of its value without contact resistance.

In order to reduce the electrical contact resistances, carbon nanotube interfaces are being considered at the semiconductor-conductor junction [12,13]. Using carbon nanotubes, reduction of the electrical contact resistance of Si- Bi_2Te_3 by an order or magnitude was observed ($2.5 \times 10^{-5} \Omega\text{m}^2$ to $2 \times 10^{-6} \Omega\text{m}^2$). Although they help in reducing electrical contact resistivity, their susceptibility to mechanical failure has prevented them from being widely used. In any case, there exists a need for accurate measurement of electrical contact resistance in TEMs.

Characterization techniques have typically been developed to measure the resistances between electrical lead contacts and transistors and hence have been mostly based on voltage and current (VI) measurements. One of the simplest contact resistance characterization techniques is comparison of two-probe and four-probe VI characteristic measurements. The two-probe technique measures the sum of the bulk and contact resistance whereas in the four-probe measurement, the contact resistance is excluded. Hence, the difference in the two resistance measurements provides the contact resistance.

Shockley proposed a characterization method for electrical contact resistivity at a semiconductor-conductor interface called the ladder network (transmission line) technique [14]. The voltage drop across rectangular contact pads of constant width is plotted as a function of the length of the pads. Based on the extrapolated length (“transfer length”) at zero potential the contact

resistance is obtained. This method was shown to be effective by Berger [15] and Schuldt [16] for thin conducting layers. Corrections to the technique were later suggested by Reeves and Harrison [17] and Makt et al. [18] for lower transfer lengths. The contact resistances in the module depend on how the contacts were made with the pellets and the overall module level resistances are different from individual contact measurements because of variation in the contact formation. The 4-point or 2-point electrical contact measurements cannot capture the net contact resistance in the modules. Hence there still exists a need for module level measurements of electrical contact resistivity.

In the present work, we develop a novel, self-consistent methodology to predict the electrical contact resistivity at a semiconductor/conductor interface in a TEM based on module-level thermoelectric measurements and simulations. The methodology is validated with experimental and analytical results. The model developed is further used to characterize the heat sink performance and the parasitic heat load (i.e., the heat leaking into the TEM from the ambient).

II. EXPERIMENTAL SETUP

An experimental test apparatus is constructed to replicate typical conditions for TEMs operating in refrigeration mode, as shown in the exploded view in Fig. 2. The setup consists of a heat source on the cold side of the TEM and a heat sink on its hot side. A patch heater attached to an aluminum heat spreader on the cold side of the TEM is used to simulate heat dissipation from an optoelectronic device. A pin fin heat sink along with a fan is used to dissipate the heat from the hot side of the TEM. The heat source/spreader assembly is insulated with Styrofoam to minimize the parasitic heat load from the ambient. A thermally conductive silicone paste (OMEGATHERM 201) is applied on the hot and cold sides of the TEM to minimize thermal contact resistances. The assembly is held under compression between two circular plastic plates for proper contact between surfaces at a constant pressure. Ventilation for the heat sink assembly is provided through an opening in the top plate.

Temperatures on the cold side (T_c) and hot side (T_h) of the TEM, as well as the ambient temperature (T_{amb}), are measured using Omega type-T thermocouples. The type-T thermocouples have an uncertainty of $\pm 0.5^\circ\text{C}$. A thermocouple surrounded by Thermal Interface Material (TIM) in a milled channel held in the heat spreader with the same TIM measures T_c . A thermocouple is inserted in a through hole in the aluminum heat sink so that its tip is flush with the flat surface of the heat sink that is in contact with the TEM. It is held in place with thermally-conductive epoxy. This arrangement aids in the measurement of temperature (T_h) on the aluminum surface. The voltage across and current through the patch heater are measured to determine the heater power to be dissipated by the TEM. The uncertainty in the measurements of voltage and current are $\pm 1\text{mV}$ and $\pm 1\text{mA}$ respectively. Two sets of experiments are performed using the above test apparatus, namely:

- i. Heat sink + parasitic load characterization
- ii. TEM characterization

The first set of experiments is performed without a TEM in between the heat sink and heat source/spreader assembly. The heat sink is in direct contact with the heat spreader via a TIM (OMEGATHERM 201). The latter set of experiments is run with the TEM sandwiched between the heat sink and the heat source/spreader assembly. These two sets of experiments help characterize the performance of TEMs, heat sink, parasitic load and interfacial Ohmic heating.

Fig. 3 shows the relevant thermal resistance in the two sets of experiments. The directions of heat flow expected in each scenario are also shown in the figure. As shown in Fig. 3a, the heat generated by the heat source flows out through two parallel heat transfer paths, namely the heat sink assembly and the foam insulation. In Fig. 3b, the heat transfer into the cold side of the TEM is a combination of the heat load applied and the parasitic heat load due to imperfect insulation. The heat transfer from the hot side is through the heat sink assembly and includes the electric power supplied to the TEM that is dissipated as heat.

Fig. 2. Exploded view of the experimental setup.

Fig. 3. Thermal network for the two sets of experiments: (a) Heat sink/parasitic load characterization, and (b) TEM characterization.

III. NUMERICAL MODEL

The numerical model is based on data from the Melcor thermoelectric library included in ANSYS Icepak [19], a finite-volume-based software package. A schematic diagram of the model is shown in Fig. 4. The thermoelectric pellets, solder tabs and the ceramic plates are included in the model. The pellet region in the TEM (containing both pellets and air) is directionally homogenized based on a perpendicular weighted mean approach and modeled as a single material with anisotropic thermal properties. The solder tabs on either ends of the pellets are also homogenized similarly. The homogenization aids in reduction of grid count and hence computational time of system-level simulations.

Fig. 4. Schematic diagram of the numerical model.

The Peltier effect, Thomson effect and Ohmic heating in the pellets are implemented through User Defined Functions (UDFs) [20]. The Peltier effect ($q''_{Peltier}$) is modeled as a planar heat source or sink at the pellet-solder tab interfaces, given by:

$$q''_{Peltier} = \frac{2NTI\alpha_{p,n}}{A_{TEM}} \quad (1)$$

where N is the number of thermocouples (pairs of n-type and p-type legs in the TEM), $\alpha_{p,n}$ is the Seebeck coefficient of a thermocouple (i.e., $\alpha_p - \alpha_n$), T is the temperature of the interface (hot or cold) and A_{TEM} is the total footprint of the TEM. Ohmic heating (q'''_{Ohmic}) and the Thomson effect ($q'''_{Thomson}$) in the pellets are modeled as bulk heat sources with volumetric heat generation rates of

$$q'''_{Ohmic} = \frac{2NI^2\rho}{A_{TEM}A_p} \quad (2)$$

$$q'''_{Thomson} = -\frac{2NTI}{A_{TEM}} \nabla \alpha_{p,n} \cdot e_p \quad (3)$$

where ρ is the electrical resistivity of the thermoelectric material and e_p is a unit vector along the axis of the pellet in the direction of current flow. The above equations are valid above the Knudsen limit and so is the modeling approach. The temperature variation in the Seebeck coefficient ($\alpha_{p,n}$) and bulk Ohmic resistivity (ρ) is also taken into account [21]. It should be noted that the heat generation occurs in the pellet and the Peltier effect occurs at the pellet-solder junctions on either side of the pellet. The temperature-dependent properties of the thermoelectric material ($\alpha_{p,n}$, ρ and k) [19] employed in the simulations are:

$$\alpha_{p,n} = -1.1784 \times 10^{-4} + 2.0147 \times 10^{-6}T - 3.6103 \times 10^{-9}T^2 + 1.5054 \times 10^{-12}T^3 \quad (4)$$

$$\rho = 3.8512 \times 10^{-8} + 1.4759 \times 10^{-8}T - 7.7117 \times 10^{-11}T^2 - 3.2395 \times 10^{-14}T^3 \quad (5)$$

$$k = 1.7906 \times 10^{-2} + 9.9351 \times 10^{-5}T - 6.4238 \times 10^{-7}T^2 + 9.6500 \times 10^{-10}T^3$$

All values are in SI units. The thermal conductivities of the ceramic (alumina) and solder materials used in the simulations are 27W/mK and 199 W/mK. Note also that the thermal conductivity of the TEM is assumed to be anisotropic with the conductivity normal to the pellet axis different from the conductivity in the plane of the pellet array, and with both conductivities taking values intermediate between that of the bulk pellets and air.

The heat conduction equation for the whole TEM, i.e., pellets, solder and ceramic, is solved with the volumetric heat generation terms specified as source terms and the Peltier effect and interfacial Ohmic heating considered in surface energy balances at the pellet-solder junctions via ANSYS Icepak. Interfacial Ohmic heating is not considered in the default implementation of ANSYS Icepak [19]. The present work takes it into account by modeling the Ohmic heating at the pellet-solder tab junctions due to the electrical contact resistance as a surface coupled heat flux boundary condition. The homogenized interfacial Ohmic heating is implemented via the UDFs [20] as:

$$q''_{cont} = \frac{2N\rho_{ec}I^2}{A_{TEM}A_p} \quad (6)$$

Fixed temperature boundary conditions (Dirichlet boundary condition) are imposed on the hot and cold sides of the TEM. The validity of the uniform Dirichlet boundary condition was verified through simulations with the heat sink geometry included in the simulation. The spreading was uniform and the difference in temperature between the hottest and the coldest temperature on the hot side was less than 0.1 °C which is within the uncertainty in temperature measurement. The remaining sides of the TEM are assumed to be adiabatic. While the present model can include heat transfer from the sides, the current implementation assumes axial heat flow in view of the good thermal insulation on the sides in the experiments. The heat loss through the sides was estimated to be less than 0.1% of the heat input, and hence axial heat flow is a valid assumption. Temperature variation along the TEM thickness extracted from one of the simulations ($T_h=46.2^\circ\text{C}$, $T_c=-14.8^\circ\text{C}$ and $I=2.4\text{A}$) is shown in Fig. 5. The temperature variation in the ceramic plates and solder is linear where as it is parabolic in the pellet region due to bulk Ohmic heating.

IV. RESULTS AND DISCUSSION

Three TEMs [21] were tested using the experimental setup described above. The parameters for each are given in Table 1. TEM_1 and TEM_2 are identical TEMs and are used to check for repeatability. The heat load and current through the TEM are varied. T_c , T_h , T_{amb} and voltage (V) across the TEM are measured. The results for TEM_1 are discussed in detail and subsequently generalized to all TEMs. The numerical model is applied for the same conditions as in the experiments, and the interfacial Ohmic resistivity, heat sink performance and the parasitic heat load are computed. The predictions are compared to the experiments and simplified analytical models.

A. Experimental Characterization

i. Heat Sink/Parasitic Load Characterization

The experiment is run for three different heat loads (2W, 4W and 6W). The difference between the temperature of the heat source (T_c) as monitored by the thermocouple in the heat spreader and that measured with the thermocouple in a through-hole of the pin fin heat sink (T_h) represents the temperature drop across the TIM. The temperature difference between the heat source and ambient ($\Delta T_{source-amb}$), and temperature difference across the TIM ($\Delta T_{source-h}$) are plotted against the applied heat load in Fig. 6a-b. The uncertainty in the temperature measurements are shown as error bars in the figure. The slope of the linear fit in Fig. 6a represents the combined heat sink + parasitic resistance in parallel. The material in between the source thermocouple and heat sink thermocouple is the thermal interface material. Hence the slope of the linear fit in Fig. 6b provides the thermal interface resistance. The heatsink + parasitic resistance and thermal interface resistance are $2.56 (\pm 0.2)$ K/W and $0.2 (\pm 0.2)$ K/W.

Table 1. Parameters for the 3 Melcor TEMs used in the study.

Parameter	CP1.0-31-05L	CP1 0-31-06L
	TEM_1, TEM_2	TEM_3
G (cm ⁻¹)	0.00079	0.00061
Cross-section of TEM	15mm×15mm	15mm×15mm
Number of thermocouples, N	31	31
Total height of TEM	3.2mm	3.58mm

Fig. 5. Temperature predictions from the TEM numerical model across the thickness for the conditions listed in Table 1.

Fig. 6. (a) Temperature of the heat source above ambient as a function of applied heat load, (b) the temperature drop across the TIM as a function of applied heat load.

ii. Module Characterization

Fig. 7 shows the power consumed by the TEM as a function of current for various heat loads. The uncertainties in the measurement of temperature ($\pm 0.5^{\circ}C$) and power ($\pm 0.3\%$) are small and hence not shown in further plots. The power consumed is insensitive to the heat load applied and a parabolic profile predicts the behavior well. Fig. 8 shows the cold and hot side temperatures of the TEM for the same conditions shown in Fig. 6. Also shown is the ambient temperature during the experiments. For a given load, T_h increases with increasing current; T_c initially decreases with increasing current, but has a minimum at $\approx 2A$ of current.

Fig. 7. Power consumption of TEM as a function of current for different heat loads.

Fig. 8. T_c , T_h and T_{amb} plotted as a function of current through TEM_1.

Hence, the TEM cannot be used when required to cool below a certain temperature is required for a given load. This current also corresponds to the current at which maximum cooling can be achieved. These trends are consistent with those expected of TEMs.

B. Interfacial Ohmic Heating

The TEM is simulated via the numerical model described earlier, with and without the interfacial Ohmic heating taken into account. The experimentally measured hot and cold side temperatures and the current through the TEM are provided as inputs to the numerical model. The heat transfer rates from the hot and cold side are obtained as outputs from the simulations as:

$$q_c = k_{ceramic} \int_{A_{TEM}} \left. \frac{dT}{dz} \right|_{z=z_c} dA \quad (7)$$

$$q_h = k_{ceramic} \int_{A_{TEM}} \left. \frac{dT}{dz} \right|_{z=z_h} dA \quad (8)$$

where z_c is the cold side z-coordinate and z_h is hot side z-coordinate. The power consumed by the TEM is estimated as:

$$\dot{W} = q_h - q_c \quad (9)$$

The power consumption predicted by the numerical simulations is compared with the experimentally measured values in Fig. 9. From the figure, it is evident that predictions that do not take interfacial Ohmic heating into account underpredict the experiments by 15%. It is expected that the interfacial Ohmic heating model would provide better predictions of the power

dissipation from the TEM. Since the electrical contact resistance of the TEM is not directly measured, it is obtained based on a match between experimental and predicted results for the power consumption. The electrical contact resistivity corresponding to the best match between the predicted and experimentally measured power consumption is $1.0 \times 10^{-9} \Omega \text{m}^2$. The sensitivity of the overall power consumption and power consumption due to just electrical contact resistance for a deviation of $0.1 \times 10^{-9} \Omega \text{m}^2$ in ρ_{ec} (10% change over baseline) are 1.5% and 10% respectively.

The electrical contact resistivity is also derived from the ΔT_{max} performance specifications for the TEM. The $\Delta T_{max} = 67^\circ \text{C}$ at $T_c = 25^\circ \text{C}$. The expression for ΔT_{max} was given by Hodes [22] to be:

$$\Delta T_{max} = \frac{\alpha^2 T_c^2}{2KR} \quad (10)$$

where K is the thermal conductance given by:

$$K = \frac{kA_p}{H_p} \quad (11)$$

and R is the total electrical resistance, which is the sum of the bulk Ohmic resistance and the electrical contact resistance of the pellet:

$$R = \frac{\rho H_p}{A_p} + \frac{\rho_{ec}}{A_p} \quad (12)$$

Here, H_p is the height of the pellet and A_p is its cross-sectional area. Averaged values of the bulk Ohmic resistivity (ρ) and thermal conductivity (k) of the pellet material are used in the calculation. The analytically-predicted value of electrical contact resistivity is $1.2 \times 10^{-9} \Omega \text{m}^2$, which agrees reasonably well the predictions from the new methodology developed in this work. Given the uncertainties in the input properties used in this analysis, the deviation between the two values is expected.

Fig. 9. Power consumption of TEM from experiments and numerical simulations.

The numerical model is applied to the remaining two TEMs to check the validity of the electrical contact resistivity deduced from the experimental results for the first TEM. Fig. 10 shows a comparison of the predicted power consumption of the TEMs against the experimentally measured values as a function of current. The numerical model clearly succeeds in predicting the experimental measurements when the electrical contact resistance is appropriately taken into account in the model.

C. *Heat sink and parasitic heat load resistances*

The methodology can further be employed to characterize heat sink resistance and parasitic heat load resistances. It is essential to characterize the heat sink resistance, as the performance of the TEM depends strongly on the heat sink performance [23]. In the present experiments, the heat transfer from the hot side (q_h) of the TEM corresponds to the heat transfer rate through the heat sink (q_{HS}).

When a given heat load is cooled using a TEM, the parasitic heat load leaking into the TEM poses an additional heat load. Hence total heat load (q_c) on the TEM is given by:

$$q_c = q_{load} + q_{para} \quad (13)$$

where q_{load} is the heat load from the source and q_{para} is the parasitic heat load from the ambient. Therefore, an understanding of the heat sink thermal resistance and the parasitic heat load resistance is necessary for proper design of TEM based refrigeration assembly.

Fig. 11 shows the results for the heat sink performance and the parasitic heat load based on the numerical simulations. In this figure, $(T_h - T_{amb})$ is plotted against the heat transfer rate from the hot side (q_h) of the TEM while $(T_h - T_{amb})$ is plotted against the parasitic heat load from Eq. (13), given by $(q_c - q_{load})$. Also included in the plots are linear fits to the data. The slopes of the linear fits provide the resistances of the heat sink and of the parasitic heat load, which are summarized in Table 2.

Fig. 10. Predicted and measured power consumption of the remaining two TEMs from experiments and numerical simulations.

Fig. 11. (a) Heat sink, and (b) parasitic heat load characteristics.

The predicted thermal resistances are further benchmarked against heat sink and parasitic load characterization experiments conducted without the TEM in place. The parallel equivalent of the heat sink thermal resistance and parasitic heat load resistance is estimated as:

$$\frac{1}{K_{parallel}} = \frac{1}{K_{HS}} + \frac{1}{K_{para}} \quad (14)$$

Table 3 shows a comparison of the predicted parallel resistance ($K_{parallel}$) and the experimentally measured value. The predictions are within 3% of the measured experimental resistance, pointing to the efficacy of the modeling approach developed in this work.

Table 2. Heat sink and parasitic heat load resistance predictions.

Parameter	Predicted thermal resistance (K/W)	R^2 value of the fit
Heat sink	2.72 ± 0.2	0.98
Parasitic heat load	32 ± 0.2	0.95

Table 3. Comparison of equivalent parallel resistance between numerical predictions and experiments.

	Thermal resistance (K/W)
Numerical ($K_{parallel}$)	2.50 ± 0.2
Experimental (from Table 2)	2.56 ± 0.2

V. CONCLUSIONS

A new thermoelectric module characterization test apparatus is developed. The test apparatus is used to characterize TEMs, thermal interface resistances and heat sink and parasitic load thermal resistances associated with hot and cold sides of the TEM. A self-consistent and novel methodology is also formulated and employed to obtain the interfacial Ohmic heating based on module-level measurements. Electrical contact resistivity is determined based on the mismatch in prediction of the TEM power consumption with and without the inclusion of an electrical contact resistance model. The methodology is further utilized to predict the heat sink and parasitic heat load thermal resistances. Predictions from the model are benchmarked against experiments and also validated against performance specifications provided by the TEM manufacturer. The electrical contact resistivity of the TEMs under consideration is predicted to be $1.0 \times 10^{-9} \Omega m^2$.

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