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THERMAL CONTACT RESISTANCE FOR A CU/G-10CR INTERFACE IN A CYLINDRICAL GEOMETRY

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ABSTRACT

A major component of a high- T_c superconductor current lead designed to provide current to low- T_c superconductor magnets is the heat intercept connection, which is a cylindrical structure consisting of an inner Cu disk, a thin-walled G-10CR composite tube, and an outer Cu ring, assembled by a thermal-interference fit. It was determined in a previous study that the thermal contact resistance (R_c) between the composite tube and the two Cu pieces contributed a substantial portion of the total thermal resistance between the inner and outer Cu pieces. The present report emphasizes the analysis of the data for the third and final design of the heat intercept connection. In particular, it is found that R_c decreases dramatically with increasing heat flux, a result consistent with earlier studies of composite cylinders. However, for the present data, the thermal contact conductance ($= 1/R_c$) varies with the calculated contact pressure with a power-law exponent of approximately 10, as compared to a theoretical value near 1. In addition, the presence of He or N_2 gas substantially reduces R_c , even though the contacting surfaces are coated with a thermal grease.

NOMENCLATURE

a, b, c	composite cylinder radii [m]
$C_1, C_2, C_3, \dots$	constants in Eq. (7)
E	Young's modulus [Pa]
h_c	thermal contact conductance ($= 1/R_c$) [$W m^{-2} K^{-1}$]
H	microhardness [Pa]
k	thermal conductivity [$W m^{-1} K^{-1}$]
L	cylinder length [m]

P	contact pressure [Pa]
q	heat flux [$W m^{-2}$]
R_c	thermal contact resistance [$m^2 K W^{-1}$]
R_{tot}	total thermal resistance across the composite tube [$m^2 K W^{-1}$]
u	displacement [m]
α	thermal expansion coefficient [K^{-1}]
β, n	constants in Eq. (3)
δ	thickness [m]
ΔT_i	temperature drop across the inner cylinder (Eq. 12) [K]
ν	Poisson ratio
σ	surface roughness [m]

Subscripts

m	harmonic mean
tube	pertaining to the composite tube
1, 2	pertaining to surfaces 1 and 2, respectively

INTRODUCTION

A promising application of high-temperature superconductors (HTS) is electrical current leads for magnets made from low-temperature superconductors (LTS). Since LTS are operated at liquid-helium temperatures (~ 4 K), and HTS at liquid-nitrogen temperatures (≤ 80 K), HTS conductors are attractive for delivering electrical current to the LTS magnet over the intermediate

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temperature range 4 K to approximately 80 K. The chief advantage of an HTS current lead is that the cryogenic refrigeration load is significantly reduced. Such HTS current leads have been designed, fabricated, and tested (Hull, 1993; Niemann et al., 1993). A critical component of these current leads is the *heat intercept connection*, which serves to electrically isolate the current-carrying interior of the leads, while at the same time providing a low-thermal-resistance link to a cold sink for heat removal (Niemann et al., 1995).

The heat intercept connections designed at Argonne National Laboratory (Niemann et al., 1995) consist of two Cu pieces—an inner disk and an outer cylindrical component—separated by a thin-walled epoxy/fiberglass composite (G-10CR) tube. The Cu pieces and the composite tube are assembled by shrink fitting, which provides a radial pressure at the two interfaces of the Cu and the tube. The original design was modified twice, and each assembly was tested for its thermal performance. The results indicated that the thermal contact resistance, R_c , between the Cu and the tube was a significant component of the overall thermal resistance of the heat intercept connection. Hence, it was deemed important to try to understand and to minimize R_c in order to optimize the design of the heat intercept connection.

Although some experimental results on the total thermal resistance of the three designs of the heat intercept connections have already been reported (Niemann et al., 1995), little attempt has been made so far to analyze these data in light of existing theoretical predictions and experimental correlations of R_c as a function of contact pressure, geometry (cylindrical), temperature (cryogenic), interstitial media (indium, gold plating, thermal grease), and surface preparation. This report presents previously unpublished data on the third and final version of the heat intercept connection. The data are analyzed by considering the effect of temperature drop on the contact pressure between the Cu pieces and the composite tube. It is found that no simple relation exists between the nondimensional contact conductance and the contact pressure.

EXPERIMENT

The experimental details are fully described in a previous report (Niemann et al., 1995), so only a brief description is given here. A schematic diagram of the experimental configuration relevant for the current discussion is presented in Fig. 1. The inner Cu disk is electrically isolated from the Cu outer ring by a thin-walled tube made from a material equivalent to G-10CR epoxy/fiberglass composite, having an approximate epoxy content of 40%. The composite cylindrical structure is held together by means of a thermal-interference fit. In the real current-lead application, the HTS leads are mounted in the Cu inner disk and extend downwards, while conventional Cu current leads are mounted in the upper portion of the Cu inner disk and extend upwards. The heat gain from the conventional leads is directed radially outward. Accordingly, in the experiment the conventional current leads are simulated by an electrical heater mounted in the center of the Cu inner disk, and the Cu outer ring is thermally connected to a mechanical cryocooler, resulting in an approximately uniform heat flux directed radially outwards.

Temperatures are measured using Type K thermocouples and silicon diodes. The thermocouples are mounted in 1-mm-diameter holes drilled to approximately the mid-length of the Cu inner disk and outer ring, and are situated to allow the temperature difference

Fig. 1 Schematic diagram of experimental apparatus.

across the composite tube to be measured at two angular orientations. The thermocouples were calibrated at a single temperature by immersing the entire structure in liquid nitrogen and comparing their outputs to that of a reference silicon diode. The resulting corrected thermocouple readings are estimated to be accurate to within ± 0.5 K. Corresponding uncertainties in R_c and in $h_c (= 1/R_c)$ are reported directly on the figures in the form of error bars.

The experimental system permitted the heat intercept connection to be operated under either vacuum conditions, or in a gaseous environment. Data are presented later in this report on the effects of N_2 and He gas on R_c near room temperature.

Pertinent details on the surface treatments and characteristics of the material interfaces shown in Fig. 1 are presented in Table 1. The reported surface finishes were supplied by the manufacturers of the tubes and the copper pieces. No grease was applied prior to assembly as the interstitial medium between both surfaces of the composite tube and the contacting Cu surfaces. The total radial interference was 58 μm , leading to a contact pressure at room temperature of nearly 43 MPa.

DETERMINING R_c FROM THE EXPERIMENTAL DATA

In our experimental configuration, the total temperature drop across the tube is measured, and from the known heater power, the

Table 1 Surface treatments and finishes of the three components of the composite cylindrical structure.

Component	Surface Treatment	Surface Finish (mm)
Cu Inner Disk	Grind, Gold Plate	0.254
G-10CR Tube	none	1.676 (inner)/ 1.981 (outer)
Cu Outer Ring	Grind, Gold Plate	0.254

total thermal resistance across the tube, R_{tot} , is calculated. The total thermal resistance includes both the thermal conduction resistance across the tube itself (R_{tube}), as well as the thermal contact resistances (R_c 's) at the inner and outer tube interfaces. We can express R_{tot} as

$$R_{tot} = R_{tube} + 2R_c \quad (1)$$

where the assumption is made that R_c is equivalent at the inner and outer tube interfaces. This assumption is justified considering that the surface treatments and finishes of the Cu inner disk and the Cu outer ring are the same, the surface finish of the composite tube is almost the same for its inner and outer surfaces, the tube thickness is only 381 μm , and the stiffness of the composite tube is small compared to the inner and outer Cu pieces, which renders the inner and outer contact pressures essentially the same. To determine R_c from our measured R_{tot} , we must know R_{tube} , which is given by the relation

$$R_{tube} = \frac{\delta_{tube}}{k_{tube}} \quad (2)$$

where δ_{tube} is the tube thickness (381 μm) and k_{tube} is the tube thermal conductivity. For this very thin tube, it is estimated that k_{tube} will differ from the bulk thermal conductivity of G-10CR, since very few woven glass layers are present in the thin tube. A microphotograph of the tube cross-section revealed the presence of 4 complete layers of glass cloth, plus an additional partial layer. Preliminary analysis suggests that this partial layer will alter the heat flow patterns within the composite material, from what would be expected with a regular array of an integer number of glass layers. Hence, k_{tube} should differ somewhat from the bulk thermal conductivity, and thus we need to estimate k_{tube} in order to extract R_c from the experimental data.

Data presented in an earlier report (Niemann et al., 1995) demonstrated that, at cryogenic temperatures, the presence of He gas reduced R_c to a negligible value compared with R_{tube} . It was also noted that R_{tot} measured in 1-bar He gas was about 18% less than R_{tube} calculated assuming the bulk value for G-10CR thermal conductivity (Hust, 1982). This 18% reduction in R_{tube} from that

Fig. 2 Effect of heat flux on thermal contact resistance.

calculated using the bulk thermal conductivity is interpreted as the size effect on the composite thermal conductivity due to the small, noninteger number of glass layers. Therefore, R_{tube} for the present data is taken to be equivalent to R_{tot} measured earlier in 1-bar He gas. Since R_{tot} was measured only to a minimum temperature of about 100 K, R_{tube} is extrapolated to the lower temperatures necessary for the analysis of some of the present data by reducing R_{tube} calculated, assuming the bulk thermal conductivity, by 18%. Finally, then, with an estimate for R_{tube} in hand, R_c can be determined from Eq. (1).

RESULTS AND DISCUSSION: VACUUM CONDITIONS

Measured values of R_c as a function of the average composite tube temperature are presented in Fig. 2. An additional parameter is the radial heat flux (q), which ranges from about 0.1 to almost 0.5 W cm^{-2} . The error bars in Fig. 2, which are determined from both temperature and heat flux measurement uncertainties, indicate that the resulting uncertainty in R_c is greatest at low temperatures and low q . In spite of these large uncertainties, Fig. 2 shows that R_c is a sensitive function of q , with R_c decreasing as q increases. Values of R_c also decrease with increasing tube temperature, a result consistent with the temperature dependencies of the thermal conductivities of both G-10CR and Cu.

It is well known that, for an interface in a cylindrical geometry, the contact pressure (P) will vary depending on q and on the temperature difference across the interface (Jones et al., 1974; Madhusudana, 1986; Lemczyk and Yovanovich, 1987; Madhusudana et al., 1989; Madhusudana and Litvak, 1990). A thermoelasticity model was employed to calculate P as a function of temperature difference (ΔT), and the results are shown in Fig. 3. This model considers the entire composite cylinder made up of the Cu inner disk, the composite tube, and the Cu outer ring. The initial contact pressure is taken to be constant; it does not vary with initial temperature (T_0), which ranges from 60 K to 120 K in

Fig. 3 Calculated contact pressure as a function of temperature difference.

the figure. As ΔT increases from 0 to a maximum value of 20 K, the differential thermal expansion experienced by the two Cu pieces results in a contact pressure that increases with increasing ΔT .

Since P changes with ΔT , and hence with q , it is natural to expect that R_c will also vary with q . Following Ochterbeck et al. (1992), a correlation between the thermal contact conductance, h_c ($= 1/R_c$), and P is assumed to take the form

$$\frac{h_c \sigma}{k_m} = \beta \left(\frac{P}{H} \right)^n \quad (3)$$

where σ is the surface roughness, defined as

$$\sigma = \left(\sigma_1^2 + \sigma_2^2 \right)^{1/2} \quad (4)$$

where σ_1 and σ_2 are the roughnesses of the two contacting surfaces, k_m is the harmonic mean thermal conductivity, defined as

$$k_m = \frac{2k_1 k_2}{k_1 + k_2} \quad (5)$$

H is the microhardness of the softer material in contact (in Pa), and β and n are coefficients to be determined by fitting Eq. (3) with the experimental data. Equation (3) is an adaptation of earlier expressions, such as that derived by Cooper et al. (1969) and Mikic (1974), in which β is a function of the mean slope of the contacting surfaces' profiles, and n is found to be near unity. However, as pointed out by Ochterbeck et al. (1992), difficulties arise in the application of the original formula, since the mean profile slope is relatively difficult to measure. Furthermore, as will be

Fig. 4 Nondimensional contact conductance as a function of nondimensional calculated contact pressure, plotted on a log-log scale.

demonstrated below, the original theoretical expression, especially the exponent $n = 1$, fails to represent the present experimental data.

Figure 2 displays the experimental data over average temperatures ranging from 70 to 120 K. In order to isolate the effect of q on R_c , we focus on data at a consistent average tube temperature of 90 K. This temperature represents roughly the average of our experimental range. Each heat flux curve in Fig. 2 includes points near 90 K, and by interpolation or extrapolation we thereby obtain six data points for R_c as a function of q at 90 K. These data points are compared with the nondimensional correlation, Eq. (3), in Fig. 4. Figure 4 is a log-log plot, and the contact pressures are determined from the curves in Fig. 2. The only visible error bar is for the left-most data point. The surface roughnesses are taken to be equal to the surface finishes reported in Table 1, giving $\sigma = 1.846 \mu\text{m}$. Using appropriate values at 90 K for Cu and for our composite tube, $k_m = 0.506 \text{ W m}^{-1} \text{ K}^{-1}$.

Determining an appropriate value for H in Eq. (3), that is, H for the softer material in contact, is difficult. Only two reports concerning the measurement of H at cryogenic temperatures have been found (Ochterbeck et al., 1992; Iwabuchi et al., 1996). At low temperatures (77–90 K), these studies are in good agreement for the microhardness of bare Cu, yielding an approximate value of 1800 MPa for the Vickers microhardness. However, the Cu pieces in Fig. 1 were electroplated, with first a 1.27- μm layer of Ni, followed by a 1.91- μm layer of Au, which should have the effect of reducing the effective H . Lambert and Fletcher (1994) showed that a 1- μm layer of vapor-deposited Au reduced H by approximately 16% for an Al sample, and by about 22% for a 2- μm layer. Judging from these results, we can estimate an approximately 20% reduction in the effective H for the Cu pieces, giving us 1440 MPa.

Much less is known about the hardness properties of the composite tube, although it is estimated that the tube is softer than the electroplated Cu pieces. Values of H for composite materials can be measured using the Rockwell M scale, which employs a large

diameter (0.25 in., or 6.4 mm) ball to distribute the load over both the relatively soft matrix and the relatively hard fibers. Typically, as the resin percentage increases, H decreases. The composite tube manufacturer reports a Rockwell M hardness of 112 kg mm⁻² for the composite at room temperature, which converts to a Brinell hardness of 110 kg mm⁻² or 1079 MPa. This compares to a room-temperature Brinell hardness for bare Cu of about 1300 MPa (Iwabuchi et al., 1996), which after a 20% reduction for the electroplating yields 1040 MPa, or almost the same as that of the composite. The microhardness of G-10CR at cryogenic temperatures has apparently not been measured. Since, as will be discussed below, our main purpose is to determine the value of the exponent 'n' which fits our experimental data, the choice of H is relatively immaterial, in that it does not impact the value of n . Hence, we employ the room-temperature value of H appropriate for the composite tube, 1079 MPa, in the following graphs. Future work will provide more insight on the microhardness of G-10CR at low temperatures, but for now we assume that the room-temperature value applies.

The data in Fig. 4 demonstrate that a simple power-law expression, like that in Eq. (3), does not describe the data well over the entire range of q . Note that there is effectively a one-to-one correspondence between the contact pressure P and q , so that low values of P correspond to low values of q , and similarly high values of P correspond to high values of q . At higher values of P/H , the data tend to fall on a straight line, in accordance with the expected relationship of Eq. (3). A best-fit line to the last three data points, which represent the range of linearity in Fig. 4, yields $n = 11.0$ for the exponent in Eq. (3). This is an extremely high value for n that, to the authors' knowledge, has not been reported previously for any kind of interface.

To the authors' knowledge, the only known data for thermal contact resistance (or conductance) for an epoxy-fiberglass composite/metal interface are reported in Marchetti et al. (1989). They examined a stainless steel/epoxy-fiberglass composite in a planar geometry. Although no correlation was applied to this set of data in their study, a brief analysis of the data points reveals a power-law exponent of $n = 1.1$, which is near the theoretical value of one.

A possible reason for the very large value of n encountered in the present study is the presence of Apiezon N thermal grease at the two interfaces between the Cu pieces and the composite tube. The thickness of the thermal grease layer may decrease with increasing contact pressure, thus bringing the thickness closer to an optimum value where R_c is minimized. Another possibility is the presence of the thin-walled composite tube between the two Cu pieces. The tube acts essentially as a relatively thick interstitial medium between the Cu pieces. An increase in contact pressure may cause the tube to deform, thus altering its heat transfer properties. Note that the calculated contact pressures in Fig. 4, around 45 MPa, are about twice those employed in Marchetti et al. (1989). Thus, local flow of the epoxy matrix may occur that would tend to enhance the thermal contact conductance. Future study will attempt to illuminate the physical basis of the large power-law exponent.

Madhusudana (1986) presented a thermoelastic analysis of a composite cylinder that directly related h_c , q , and P . A comparison between this theory and the present 90-K data is given in Fig. 5. This theory is utilized since it is much easier to apply than the competing model of Lemczyk and Yovanovich (1987), and the uncertainties in the experimental data and material properties (chiefly H) do not justify more rigorous or complex calculations.

Fig. 5 Contact conductance as a function of heat flux.

Briefly, the theory considers the total displacement (u) of the composite cylinder as the sum of three terms:

$$u = u_q + u_{\Delta T} + u_i \quad (6)$$

where u_q is the displacement due to the heat flux, $u_{\Delta T}$ that due to the temperature drop across the interface, and u_i is the initial interference. Madhusudana (1986) derived expressions for each term, and combined them in a form similar to the following:

$$\alpha_1 \Delta T_i \left[(C_4 - C_5 C_6 C_7) + \frac{C_{10}(1 + C_7)}{2 \ln \left(\frac{b}{a} \right) \left[\beta \left(\frac{P}{H} \right)^n \right]} \right] + \frac{u_i}{b} \quad (7)$$

$$= \left(\frac{P}{E_1} \right) \left[C_1 (C_2 + \nu_2) + (C_3 - \nu_1) \right]$$

where α , E , ν , and k are the thermal expansion coefficient, Young's modulus, Poisson's ratio, and thermal conductivity, respectively; the subscripts 1 and 2 denote the inner and outer components of the composite cylinder, respectively; and the various constants are defined as

$$C_1 = \frac{E_1}{E_2} \quad C_2 = \frac{c^2 + b^2}{c^2 - b^2} \quad C_3 = \frac{b^2 + a^2}{b^2 - a^2} \quad (8)$$

$$C_4 = \frac{1}{2 \ln \left(\frac{b}{a} \right)} \left[1 - \left(\frac{2a^2}{b^2 - a^2} \right) \ln \left(\frac{b}{a} \right) \right] \quad (9)$$

$$C_5 = \frac{1}{2 \ln\left(\frac{c}{b}\right)} \left[1 - \left(\frac{2c^2}{c^2 - b^2} \right) \ln\left(\frac{c}{b}\right) \right] \quad (10)$$

$$C_6 = \frac{\ln\left(\frac{c}{b}\right)}{\ln\left(\frac{b}{a}\right)} \quad C_7 = \frac{\alpha_2 k_1}{\alpha_1 k_2} \quad C_{10} = \frac{\sigma}{b} \quad (11)$$

The variables a , b , and c are the inner, intermediate, and outer radii of the composite cylinder, respectively, and for the present case have the following values:

$$a = 5.953 \text{ mm} \quad b = 31.750 \text{ mm} \quad c = 47.625 \text{ mm} \quad (12)$$

The theory was derived for a composite cylinder consisting of only two cylindrical components: an inner cylinder, and an outer cylinder (Madhusudana, 1986). However, in the present case we have three components in the composite cylinder: the Cu inner disk, the composite tube, and the Cu outer disk. We adapt our configuration to the theory of Eq. (7) by considering only the two Cu pieces, and neglecting any displacement occurring due to the thin-walled composite tube. The microhardness (H), however, at the interface between the Cu pieces is taken to be equal to that for G-10CR, so that the relative softness of the composite cylinder is taken into account. Furthermore, note that Eq. (7) differs slightly from the original expression in Madhusudana (1986) in that h_c is assumed to vary with P in the form of Eq. (3) (i.e., β and n are undefined), rather than taking specified values for these constants, as was done in the original report. This allows us to fit Eq. (7) to our present data through an iterative process by which we determine the best values of β and n .

The temperature drop ΔT_i is that across the inner cylinder. We desire to express our results as a function of heat flux (q), so we relate q to ΔT_i using the standard expression

$$\Delta T_i = \left[\frac{\ln\left(\frac{b}{a}\right)}{2\pi L k_1} \right] q \quad (12)$$

where L is the cylinder length ($= 63.5$ mm). Other properties utilized in the calculation are those for Cu at 90 K (Landolt-Börnstein, 1966; AIP Handbook, 1972):

$$\begin{aligned} k_1 &= k_2 = 476.4 \text{ W m}^{-1} \text{ K}^{-1} \\ \alpha_1 &= \alpha_2 = 9.3 \times 10^{-6} \text{ K}^{-1} \\ E_1 &= E_2 = 71.89 \text{ GPa} \\ \nu_1 &= \nu_2 = 0.415 \end{aligned}$$

The theoretical curve in Fig. 5 is that fitted through only the rightmost three data points, as was done in Fig. 4, since these three

Fig. 6 Total thermal resistance under vacuum and gaseous conditions, near room temperature.

points apparently represent the range of data where Eq. (3) can be applied. Here, the resulting value of n is slightly lower: $n = 9.5$. Still, the resulting value of n is extremely high, and future work must explore this more thoroughly, perhaps with more controlled experimentation on thermal contact conductance between metals and fiber-composite materials.

RESULTS AND DISCUSSION: GASEOUS VS. VACUUM ENVIRONMENT

The final figure, Fig. 6, shows our few measured data points which directly compare the influence of an interstitial gas on the total thermal resistance, R_{tot} . The results are left in the form of R_{tot} since we do not have an adequate means of estimating R_{tube} for the temperatures shown in Fig. 6, which are near room temperature. The large uncertainties reflect the fact that both the measured temperature drops and the applied heat fluxes are relatively small. The curve for R_{tube} presented in the figure is calculated using Eq. (2) and the thermal conductivity reported for a bulk sample of G-10CR having an epoxy content of 40% (Hust, 1982). As indicated, R_{tot} is highest for the composite cylinder operated under vacuum conditions, followed by operation in 1-bar N_2 gas, and finally operation in 1-bar He gas. The large effect of the interstitial gas on R_{tot} is somewhat surprising, considering that the interface was initially coated with Apiezon N thermal grease. Because of the grease, it was anticipated that the presence of a gas, whether N_2 or He, would have relatively little effect. Such is clearly not the case, but this may be because some of the grease was inadvertently removed during the assembly process, which makes it difficult to estimate the consistency of the coating thickness. Note that the conductance through the gas is estimated to be negligible compared with the conductance through the composite tube. Nevertheless, the presence of He gas results in a greater reduction in R_{tot} as compared to the presence of N_2 gas. This is consistent with the thermal conductivities of the two gases at 300 K and 1 bar: $k = 0.155 \text{ W m}^{-1} \text{ K}^{-1}$ for He (McCarty, 1972), and $k = 0.027 \text{ W}$

$m^{-1} K^{-1}$ for N_2 (Jensen et al., 1980). Thus, it must be concluded that a gaseous interstitial medium substantially lowers R_c , even when a thermal grease is applied at the contact.

CONCLUSIONS

The thermal contact resistance (R_c) at the interfaces in a composite cylinder constructed of an inner Cu piece, a thin-walled G-10CR epoxy-fiberglass composite tube, and an outer Cu piece was investigated, primarily at cryogenic temperatures. The results indicate a substantial influence of the heat flux on R_c , with increasing heat flux causing lower values of R_c . Analysis of the data with our own thermoelastic model and a model taken from the literature (Madhusudana, 1986) reveals that the thermal contact conductance, which is the inverse of R_c , varies with the nondimensional contact pressure with a power-law exponent of 9.5 or 11.0, depending on which model is utilized. These large values for the exponent are possibly due to the presence of the thin-walled composite tube, and/or the presence of thermal grease at the contact, but further work is necessary to understand this phenomenon. A few measurements taken near room temperature, but with the experimental apparatus surrounded by 1-bar He or N_2 gases, demonstrate that the presence of the interstitial gas dramatically lowers R_c , even though the contacting surfaces are initially coated with a thermal grease.

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REFERENCES

- American Institute of Physics Handbook, 3rd Ed., 1972, D.E. Gray, Ed., AIP, New York, pp. 4-122 - 4-123.
- Cooper, M.G., Mikic, B.B., and Yovanovich, M.M., 1969, "Thermal Contact Conductance," *International Journal of Heat and Mass Transfer*, Vol. 12, pp. 279 - 300.
- Hull, J.R., 1993, "High-Temperature Superconducting Current Leads," *IEEE Transactions on Applied Superconductivity*, Vol. 3, pp. 869 - 875.
- Hust, J.G., 1982, "Thermal Conductivity of G-10CR and G-11CR Industrial Laminates and Their Component Parts at Low Temperatures," *Materials Studies for Magnetic-Fusion-Energy Applications at Low Temperatures V*, J.G. Hust, R.P. Reed, & N.J. Simon, Eds., NBS Report NBSIR-82-1667, pp. 411 - 427.
- Iwabuchi, A., Shimizu, T., Yoshino, Y., Abe, T., Katagiri, K., Nitta, I., and Sadamori, K., 1996, "The Development of a Vickers-Type Hardness Tester for Cryogenic Temperatures Down to 4.2 K," *Cryogenics*, Vol. 36, pp. 75 - 81.
- Jensen, J.E., Tuttle, W.A., Stewart, R.B., Brechna, H., and Prodehl, A.G., 1980, *Brookhaven National Laboratory Selected Cryogenic Data Notebook*, BNL 10200-R, Vol. 1, p. VI-G-2.1.
- Jones, A.M., O'Callaghan, P.W., and Probert, S.D., 1974, "Effect of Interfacial Distortions on the Thermal Contact Resistance of Coaxial Cylinders," *Heat Transfer with Thermal Control Applications*, Vol. 39, *Progress in Astronautics and Aeronautics*, M.M. Yovanovich, Ed., pp. 21 - 44.
- Lambert, M.A., and Fletcher, L.S., 1994, "Metallic Coatings for Enhancement of Thermal Contact Conductance," *Journal of Thermophysics and Heat Transfer*, Vol. 8, pp. 341 - 348.
- Landolt-Börnstein, 1966, New Series, Group III, Vol. 1, K.-H. Hellwege and A.M. Hellwege, Eds., Springer-Verlag, Berlin, p. 21.
- Lemczyk, T.F., and Yovanovich, M.M., 1987, "New Models and Methodology for Predicting Thermal Contact Resistance in Compound Cylinders and Finned Tubes," *Heat Transfer Engineering*, Vol. 8, No. 2, pp. 35 - 48.
- Madhusudana, C.V., 1986, "On Heat Flow Across Cylindrical Joints," *Heat Transfer 1986, Proceedings of The Eighth International Heat Transfer Conference*, C.L. Tien, V.P. Carey, and J.K. Ferrell, Eds., pp. 651 - 658.
- Madhusudana, C.V., Fletcher, L.S., and Peterson, G.P., 1989, "Thermal Conductance of Cylindrical Joints—A Critical Review," *Journal of Thermophysics*, Vol. 4, No. 2, pp. 204 - 211.
- Madhusudana, C.V., and Litvak, A.L., 1990, "Thermal Contact Conductance of Composite Cylinders: An Experimental Study," *Journal of Thermophysics*, Vol. 4, No. 1, pp. 79 - 85.
- Marchetti, M., Testa, P., and Torrisi, F.R., 1989, "Measurement of Thermal Conductivity and Thermal Contact Resistance in Composite Materials for Space Applications," *International Journal of Materials and Product Technology*, Vol. 4, No. 4, pp. 379 - 388.
- McCarty, R.D., 1972, "Thermophysical Properties of Helium-4 from 2 to 1500 K with Pressures to 1000 Atmospheres," *NBS Technical Note 631*, National Bureau of Standards, Boulder, Colorado, p. 49.
- Mikic, B.B., 1974, "Thermal Contact Conductance; Theoretical Considerations," *International Journal of Heat and Mass Transfer*, Vol. 17, pp. 205 - 214.
- Niemann, R.C., Cha, Y.S., Hull, J.R., Buckles, W.E., and Daugherty, M.A., 1993, "High-Temperature Superconducting Current Leads for Micro-SMES Application," presented at the *13th International Conference on Magnet Technology*, Victoria, B.C., Canada, September 20 - 24.
- Niemann, R.C., Gonczy, J.D., Phelan, P.E., and Nicol, T.H., 1995, "Design and Performance of Low-Thermal-Resistance, High-Electrical Isolation Heat Intercept Connections," *Cryogenics*, Vol. 35, No. 11, pp. 829 - 832.
- Ochterbeck, J.M., Peterson, G.P., and Fletcher, L.S., 1992, "Thermal Contact Conductance of Metallic Coated BiCaSrCuO Superconductor/Copper Interfaces at Cryogenic Temperatures," *Journal of Heat Transfer*, Vol. 114, pp. 21 - 28.