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May 23, 1994

Ronaldo Jenkins
Mail Stop 7 E 4, OWFN
Electrical Engineering Branch
Office of Nuclear Reactor Regulation
U. S. Nuclear Regulatory Commission
Washington, DC 20555

Dear Ronaldo,

Attached for your consideration are additional comments regarding the TU response to your RAI on their ampacity derating tests (reference J2017, Task Order 1). In particular, the following comments address Questions 1, 4 and 10. The utility response to these questions was not initially assessed by Sandia as these questions had been raised by the USNRC directly. However, as per your request, I have reviewed the relevant documentation of each issue raised and am providing the attached observations for your consideration.

Sincerely

Steve Nowlen

Steven P. Nowlen
Component and Structures Safety
and Reliability Department

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ASSESSMENT OF TEXAS UTILITIES RESPONSE TO USNRC RAI ON AMPACITY DERATING TEST AND ANALYSES

Introduction

This document provides the results of a Sandia National Laboratories (SNL) review of the Texas Utilities (TU) document TXX-94085. This TU document provides utility responses to ten specific questions asked by the USNRC in regards to certain utility submittals regarding tests and analyses associated with ampacity derating of Thermo-Lag protected conduits and cable trays. SNL has been asked to assess the adequacy of the TU response to resolve the questions raised.

An initial assessment was provided in a letter from S. Nowlen to R. Jenkins dated April 22, 1994. This initial assessment addressed questions 2, 3, 5, 6, 7, 8, and 9, those questions raised as a result of SNL review of the utility submittals. The following sections provide a question by question assessment of the response adequacy for the remaining questions, namely, questions 1, 4 and 10. These questions were raised as a direct result of NRC reviews of the subject documents.

Question 1

This question raised three points dealing with the testings compliance with the IEEE P848 draft test standard acceptance criteria. These points, and the adequacy of the utility response are as follows:

Point 1: The first point was associated with compliance with the criteria for establishing a Location 2 hot spot temperature of 90±1.1°C. It appears that the utility tests are in compliance with the IEEE standard procedures in this regards with one possible exception. Even in the one suspect case, SNL considers the utility analysis to be acceptable and conservative. Note in particular that the standard does not require that these conditions be maintained for a full three hours, but rather, requires only that once the equilibrium conditions are achieved that the hot spot temperature remain within this band. In fact, the utility tests do demonstrate a period of at least one hour in each test during which this temperature criteria is met. This appears sufficient to demonstrate that the utility tests did achieve a steady state condition.

The only possible exception to this observation was the clad test involving a 2" conduit (Scheme AC-4). In this case, the location 2 hot spot temperature at the end of the test was outside the 90±1.1°C limits. In fact, the hot spot temperature fluctuated throughout the last hour of the test, ranging from a low of 87.2 to a high of 89.4. The hot spot temperature recorded in the final scan was 87.2°C. However, the running one-hour average temperature at this time did remain quite flat and within the specified limits, the final value being 89.1°C.

In this case, the interpretation of the standard is somewhat at issue. It is not explicitly stated in the standard whether the 90±1.1°C criteria should be applied to the individual hot spot temperature measurements or to the running average hot spot temperature. The standard does include the statement "The degrees of freedom obtained by averaging many thermocouple locations ... allows

the precision to exceed the ±1.1°C limit of any one thermocouple" (section 5.4.3, para. 2). This statement appears to imply that the ±1.1°C limit is in fact to be applied to "any one thermocouple." Hence, the utility test would appear to be in violation of this criteria. An alternate interpretation of the standard would be that the running average value must remain within a band of 90±1.1°C. This interpretation allows for short term fluctuations outside of the 90±1.1°C limit, so long as the long term average remains stable and within the specified range. Under this interpretation, the utility tests would be in compliance with the standard.

It should also be noted that in this particular case the utility analysis is the more conservative interpretation. That is, because the final single point temperature value in the clad test was somewhat lower than the running average value, the corrected ampacity calculated using the single point value would have been higher than that calculated using the running average value. Because this is a clad case, a higher corrected ampacity would imply a less severe derating factor. The utility analysis was based on the use of the running average temperature, and hence, is more conservative. SNL does not consider the point-to-point fluctuations observed during this test to be of significant concern, and recommends that the utility analysis be accepted. However, it is recommended that the NRC request that IEEE clarify the intent of the standard in this regards.

Point 2: The second point was associated with the failure of the utility to establish that the average of the two side location temperatures (Locations 1 and 3) was within ±4°C of the average temperature at the center location (Location 2). The utility asserts in its response that sufficient data is provided to perform the calculation and that no test anomalies were observed. SNL has examined the final temperature scan recorded during each of the base line and clad tests. In our analysis, the final data point values were averaged for each location and the results compared to the IEEE criteria.

Based on examination of each test set, the only anomaly identified was associated with the base line test of Scheme AT-1, the 24" wide cable tray. For this test it was found that the average temperature of all of the 26 location 1 and 3 thermocouples during the last scan was 88.0°C whereas the average of 13 thermocouples at location 2 was 81.5°C, a net difference of 6.5°C which exceeds the IEEE criteria. (In this case both of the two side locations were hotter than the central location.) In all of the other cases, the temperature averages during the last scan were within the IEEE ±4°C criteria.

The objective of the side temperature deviation criteria is to ensure that a relatively uniform temperature field is established so as to minimize the effects of lateral heat transfer. In general, if the sides are cooler than the center, then heat will be conducted away from the center which would potentially result in an over-estimate of the heat rejection capacity of the center region, and hence, an over-estimate of the actual current capacity. In contrast, if the sides are hotter than the center, then heat is conducted into the center region, and an under-estimate of the actual current capacity could result.

In the case of the AT-1 base line test, the fact that the side locations were hotter than the center would imply a non-conservative configuration for a base line test. That is, the base line ampacity of the cables may have been under

estimated consistent with the above discussion. An increase in the base line current would result in a more restrictive ampacity derating factor provided that the clad test ampacity remained the same.

(Also note that in the case of Test Scheme AA1-1, the clad test article, the right side of the test article was 5.4°C hotter than the center, based on the average temperature. However, in this case the left side was slightly cooler so that the average of Sections 1 and 3 together was within the IEEE limits as compared to the average at Location 2. The IEEE draft 11 standard appears to require that the location 1 and 3 thermocouples be averaged as a single group. Hence, this test would not be considered anomalous.)

Point 3: The third point was associated with compliance with the criteria for demonstrating steady state conditions have been achieved in testing. The utility response to this issue appears adequate.

That is, the IEEE P848 acceptance criteria require that two conditions be met independently to demonstrate that and equilibrium condition has been achieved. First, a minimum time period of three hours must pass after any changes in the cable current are made. However, this does not imply that equilibrium conditions must be maintained for this full three hour period, but rather, only that no further changes in cable current be made during this period (adjustments to maintain the desired current are allowed). As a supplemental and independent condition, the slope of the temperature data is to be calculated based on a running analysis of the latest one-hours data and certain threshold levels are established to demonstrate that equilibrium is achieved. Here again, the temperature slope need not be maintained for any given period of time, but rather, must be achieved before equilibrium is declared and the final data is recorded.

All tests did provide a minimum three hour period of data recording apparently without adjustment of the amperage. The IEEE standard does not require that steady state conditions prevail for a full three hour period, but rather, specifies a minimum three hour period in which the current flow through the cable system is maintained at a constant level. The utility performance in this regards appears consistent with the standard requirements.

The tests also demonstrated, in general, a period of one hour during which the IEEE steady state condition (temperature slope condition) was met and maintained. This actually appears to exceed the IEEE draft standard in this regards. The standard requires only that the temperature slope condition be achieved before the end of the test, not that it be maintained for three hours. The standard states that "As soon as the absolute value of the slope of these data becomes less that 0.55 (conduit) or 0.35 (tray), equilibrium has been reached." The three hour time requirement is only associated with the minimum time after ampacity adjustments are made before a test can be concluded. These are parallel and complimentary requirements, not simultaneous requirements.

In summary, the TU tests appear to have complied with the criteria established in the IEEE P848 Draft 11 test standard with two exceptions as described above.

Question 4

This question was associated with the observation that the central location 2 measurements points were not always the hottest location in the tray, and questions the appropriateness of using the central location hot spot in lieu of the overall test article hot spot as the basis for analysis. The IEEE P848 Draft 11 test standard clearly states that the hot spot at the central location 2 measurement point is to be used as the basis for all calculations. The standard also explicitly allows for the side locations to be hotter than the center in that a ±4°C tolerance is allowed. Hence, the TU tests are in compliance with the standard in this regards (with the exceptions described above).

This approach is intended to provide for a direct comparison of a given location in the test specimen under open and protected conditions. The use of the location 2 hot spot temperature, regardless of the side location temperatures, provides for this consistent comparison basis. The only purpose of measurements at the side locations at all is to ensure that a lateral stability has been achieved in the test article.

In the case of the TU tests, the standard analysis procedure generally results in more conservative ampacity derating factors than would be achieved if the overall hot spot temperature were used as the basis for calculation in each individual test. As discussed above, SNL has examined the data associated with the final scan taken in each of the individual tests (that scan used as the basis for analysis by the utility). Using the temperature/ampacity correction equation with a higher temperature would result in a lower corrected ampacity. Hence, the net effect of using the hottest overall test article temperature rather than the lower center hot spot temperature would be to reduce the corrected ampacity associated with each test. In general, we found that the temperature variation problems were much more pronounced in the base line tests than they were in the clad tests. Hence, the base line ampacities would be reduced by a greater percentage than would the clad ampacities. This would result in the calculation of less severe ampacity derating factors than those calculated using the standard analysis methodology. Based on this assessment, the utility calculations would be considered the more conservative for these tests. Use of the overall hot spot temperatures as the basis for analysis would result in reduced conservatism.

The only exception to this observation would be in the case of test scheme AA1-1, the 3 conductor, 6AWG air drop. In this test, the hot spot in the base line test was at the central location, and both of the two side locations were slightly cooler. However, in the clad test the left and center locations were quite consistent, but the right side location was significantly hotter. In the clad test, the center location hot spot was 90.9°C, as compared to an overall hot spot temperature of 96.5°C. In the case of this test scheme, if the overall hot spot temperature were used as the basis for analysis, the corrected clad case ampacity would be reduced while the corrected base line ampacity would not change. Hence, a more restrictive ampacity derating factor would be calculated.

The use of a common measurement point in both the base line and clad tests would appear to be the most appropriate approach possible. In certain cases, this may result in using a temperature which is not the overall hot-spot temperature. However, the use of teperatures at the side locations may introduce unwanted end effects (such as end and

lead wire insulating effects) not representative of in-plant conditions. The central location would be expected to be the most representative location in the test article as it would be least impacted by end effects. Changing this provision would require a significant change in the draft test standard. As it currently stands, the TU tests are in compliance with the test standard in this regards.

Question 10

Question 10 was associated with the issue of inductive currents in conduit testing. The utility response states the presence of inductive currents results in conservative derating factors because "the additional heat added by the inductive heating ... reduces the ampacity of the cables." While this is true on a test-by-test basis, this assessment fails to consider that ampacity derating is based on the relative change in ampacity due to addition of the cladding. While inductive currents will generally reduce the measured ampacity in a given conduit test, the relative impact of this effect on the clad versus unprotected case is the critical factor. The utility response in this regards is considered inadequate. In order to demonstrate conservatism, the utility must show that the relative reduction in ampacity due to addition of the cladding is either not affected by the inductive currents, or that the inductive currents would result in a greater relative ampacity reduction. No such supporting analyses or experiments have been presented.

An Experimental Assessment of Thermal Conductivity in a Composite Electrical Cable Mass

Enclosure 3

Sandia National Laboratories

P.O. Box 5800
Albuquerque, New Mexico 87185-0737

January 20, 1995

Ronaldo Jenkins
Mail Stop 7 E 4, OWFN
Electrical Engineering Branch
Office of Nuclear Reactor Regulation
U. S. Nuclear Regulatory Commission
Washington, DC 20555

Dear Ronaldo,

Subject: JCN J2018, Task Order 1, Cable Thermal Conductivity Tests

Attached for your consideration is a report on the recently completed cable bundle thermal conductivity tests performed as a part of Task Order 1 of JCN J2018. The results are quite interesting, and indicate that previous estimates of this parameter were somewhat to high. Using a higher value for this parameter in a thermal model would increase the predicted absolute cable ampacity limits for any given individual case. When one considers the relative impact of a fire barrier on ampacity, the results would largely "wash out" in the ampacity ratio.

The test results also reinforce the findings of Ron Dykhuizen in his thermal modeling work. As you may recall, Ron was forced to use a lower value of the cable mass thermal conductivity as compared to the two values reported by Stolpe and by Engmann in order to achieve a good match between certain 12AWG, 3-conductor cable tray ampacity experiments and his model predictions (see work under JCN J2018 Task Order 2). It was quite rewarding for us to find that our experiments with a 12AWG, 3-conductor cable bundle gave thermal conductivity values quite similar to those used by Ron (an experimental value of 0.087 BTU/ft/hr/°F as compared to Ron's assumed value of 0.08 BTU/ft/hr/°F, Stolpe's assumed value of 0.15 BTU/ft/hr/°F, and Engmann's assumed value of 0.12 BTU/ft/hr/°F). This result indicates that Ron's thermal model was giving very reasonable ampacity prediction results, and that his earlier concerns regarding the assumed thermal conductivity values were well justified.

You will note that the attached report is in a format similar to a conference paper or journal article. I would be interested in pursuing publication of these results. We have also previously discussed publishing Ron's thermal model work through IEEE. I would suggest that publication of both works now be pursued. This would also provide for a firm basis for referencing the two

works. Because of the fundamental differences in the nature of the two works (analytical versus experimental) maintaining a separation as two different papers would be appropriate and, given the current status of each work, would actually help to minimize the costs (one person could easily present both papers if a conference presentation is called for, and both works have been independently prepared in a similar journal-style format). Please let me know if you are still interested in pursuing publication through IEEE.

Sincerely

Steven P. Nowlen
Component and Structures Safety
and Reliability Department

1 Attachment: "An Experimental Assessment of Thermal Conductivity in a Composite Electrical Cable Mass," January 20, 1995.

An Experimental Assessment of Thermal Conductivity in a Composite Electrical Cable Mass[1]

January 20, 1995

Steven P. Nowlen
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Abstract

The problem of heat transfer within a mass of electrical cables is currently the focus of considerable interest, in particular, as associated with the protection of cable trays and conduits by a fire barrier system. The use of such systems requires that cable ampacity limits be reduced in order to compensate for the insulating effects of the fire barrier system. The extent to which cables must, in this manner, be "derated" can be assessed either experimentally or through analysis. Using the analytical approach, one of the critical parameters which must be assessed in the effective thermal conductivity of the composite cable mass. This paper presents the results of experiments performed to determine this value for two specific cable mass bundles.

Introduction and Overview

Modeling the heat transfer behavior of electrical cables housed in a routing system such as a cable tray or conduit is a topic of recent interest. In particular, the assessment of changes in the heat transfer behavior which result from the addition of a fire protective barrier system has been the focus of considerable recent effort in the nuclear industry. This interest is based on a need to assess the ampacity limits of protected cables so as to ensure that cable temperatures remain at or below the qualified lifetime exposure temperatures of the cable insulation materials (typically 90°C).

A cable mass is a composite of copper (or aluminum) conductor, some type(s) of insulation, jacketing and binder material(s) (typically plastics, silicone based materials, and/or rubber-based materials), and air (in the gaps between cables and between the individual strands which make up the conductors). In order to model the heat transfer behavior for such a system, one must either resort to detailed two-dimensional models which address each of these individual constituents as separate bodies, or one must simplify the problem. Clearly, the detailed modeling approach will be quite

[1]This work was performed for the USNRC under Task Order 1 of JCN J2018 and provides an account of as yet unpublished SNL test results.

complicated, and will introduce numerous case specific factors which will be very difficult to either control or characterize. In the case of simplification, the common approach is to model the cable mass as an equivalent homogeneous mass thus reducing the problem to a simple one-dimensional heat transfer problem. In this case, one of the critical parameters which must be evaluated is the equivalent thermal conductivity of the composite cable mass.

The equivalent thermal conductivity plays an important role in the determination of both the location and magnitude of the thermal "hot-spot." The hot-spot is of primary interest because it represents the worst-case cable operating condition. In a recent review[2] it was found that the assumed value of this parameter was critical to the prediction of both actual absolute cable ampacity limits and, to a lesser extent, also impacted the predictions of the relative impact of a fire barrier system on those ampacity limits. However, no basis for the values assumed in previously published studies could identified.

The equivalent thermal conductivity of the cable mass can not be easily estimated. For example, one cannot simply estimate this value based on a simple weighted average of the constituent elements. Using such a simple method to assess the 8AWG cable would give values of on the order of 100 W/m/°K. (Copper makes up approximately 30% of the overall cross section, and has a conductivity of approximately 385 W/m/°K. Hence the copper's contribution to the weighted average alone would exceed 100 W/m/°K (0.3 x 385).) A method such as this would effectively treat the individual elements as parallel path thermal resistors. Such simplistic approaches are inadequate to address the complex behaviors involved in the two-dimensional heat transfer process which actually characterizes the system.

This paper presents the results of tests performed by Sandia National Laboratories (SNL) to determine the equivalent thermal conductivity for two different cable bundles. Transient heat transfer tests in a cylindrical geometry were used as the basis for the conductivity measurement.

Basic Experimental Approach

The basic technique used in these experiments is known by various names. These include the Van de Held method, the Stalhane Pyk method, and the d'Eustacio probe method. Fundamentally, the technique is based on monitoring the transient temperature response of a line heat source (or heat probe) immersed in an infinite homogenous medium. When constant power is supplied to the heating probe, the

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[2]This study was performed under Task Order 2 of USNRC JCN J2018 and is documented in a letter report to the USNRC dated 10/24/94 entitled "Fire Barrier System Cable Ampacity Derating; A Review of Experimental and Analytical Studies"

temperature rise of the heater probe itself is a function of the thermal conductivity of the surrounding material. Numerous analyses have been performed for this arrangement, the simplest being given by Carslaw and Jeager [1]. For a probe of perfect conductance, without contact resistance, and ignoring higher order terms in the solution, the thermal conductivity of the surrounding medium is given by

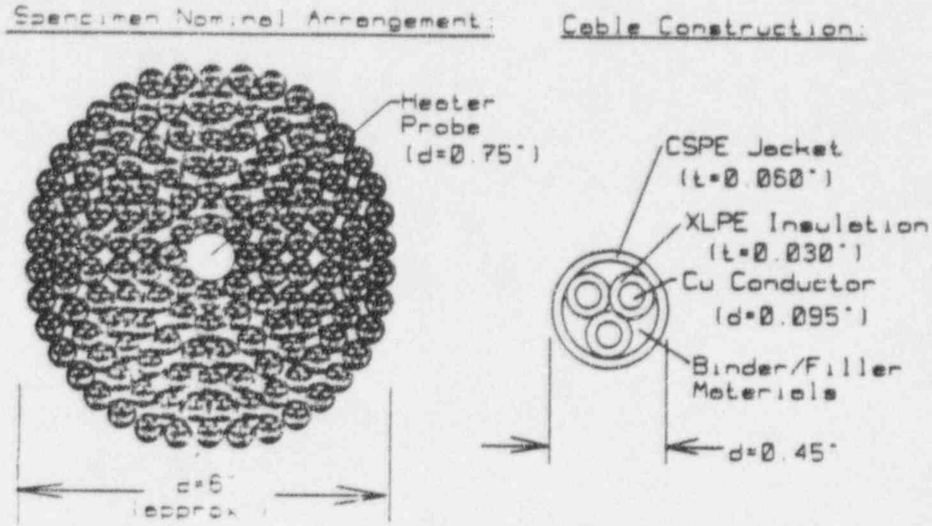
$$k = q\left[\frac{\ln\left(\frac{t_2}{t_1}\right)}{4\Pi(\theta_2-\theta_1)}\right]$$

where (k) is the thermal conductivity of the tested medium (W/m/°K), (q) is the probe power per unit length (W/m), (t) is the time (in seconds), and (θ) is the temperature (°K). The subscripts 1 and 2 refer to arbitrary choices of two time-temperature data pairs. In effect, this equation suggests that conductivity is inversely proportional to the slope of the time-temperature curve when plotted on a log-normal scale.

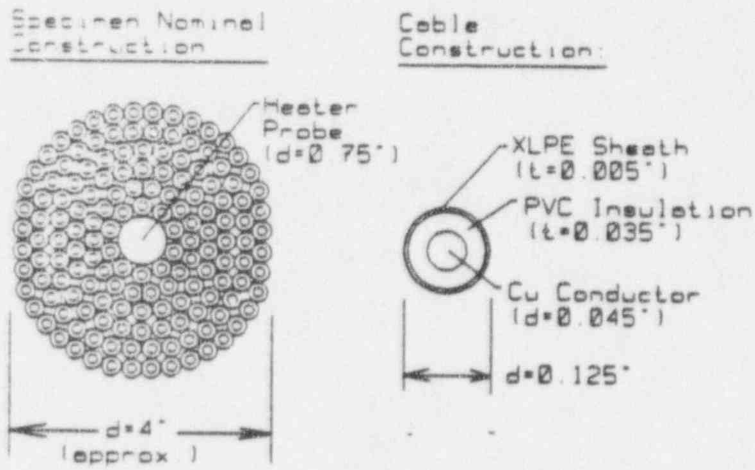
Test Specimen Construction

Two test specimens were constructed for use in these experiments. In each case, the test specimen was constructed around a centrally located resistance heating rod measuring approximately ¾" in diameter by 36" long. Lengths of the cable of interest were cut to 36" and secured to the test specimen so as to completely surround this heater rod. In particular, cables were added in concentric ring layers such that a very tight cable-to-cable spacing was achieved. Each progressive ring of cables was secured to the specimen using 24ga stainless steel wires. The wires were typically spaced at 8-12" intervals along the length of the specimen. These wire ties were also offset between adjacent cable layers. A total of six such layers were installed for each of the two test specimens.

Two types of cable were investigated. The first was an 8AWG single conductor, 600V cable with a 35 mil (0.035") Polyvinyl-Chloride (PVC) insulation and a 5 mil cross-linked polyethylene (XLPE) outer sheath. The second cable was a 3-conductor, 12AWG, 600V cable with a 30 mil XLPE insulation on each conductor and with a 60 mil chloro-sulfonated polyethylene (CSPE or Hypalon) over-jacket. The multi-conductor 12AWG cable also included nylon strands and a nylon sheath used as binding/filler materials in the gaps between the individual conductors and the jacket (thus a basically round profile is maintained for this cable). Both cables used stranded copper conductors (a standard 1-6-12 stranding pattern was used in the formation of the conductors for both cable sizes). Figure 1 illustrates the cable construction and the nominal layout of cables in each of the two cable bundle test specimens.



(a) 12 AWG, 3-conductor cable bundle.



(b) 8 AWG, 1-conductor cable bundle.

Figure 1: Nominal configuration of cable bundle thermal conductivity test specimens. Note location of heater probe at the center of each bundle.

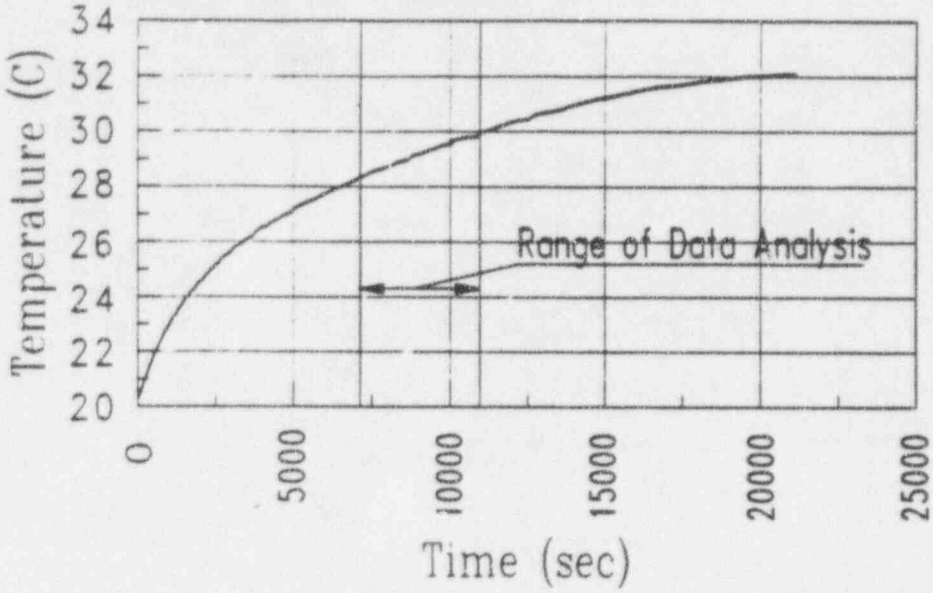
Data Analysis and Results

In the experiments described here, the centrally located heater rod represents the line source, and the cables the surrounding medium. While the medium in this case is far from homogeneous, the intent is to provide a relative composite thermal conductivity. Given this understanding, the non-homogeneous nature of the cable mass is not of fundamental concern. It should also be noted that the test specimens were each of limited size rather than of infinite extent. However, the data analysis routinely performed includes a check for indications of size effects. That is, if the heating penetrates to the outer surface of the cable mass in any significant quantity, then this would be reflected in the data as a change in the time-temperature curve slope. These and other similar issues have been explored by Drotning and Tormey [2].

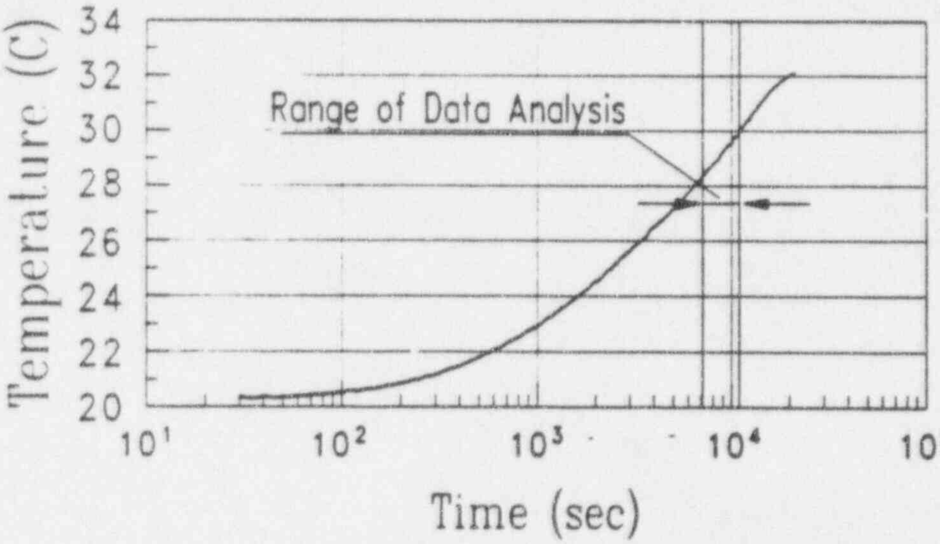
Figures 2 illustrates a typical data set from an individual experimental run (the data presented is actually for the 12AWG cable bundle). The data has been presented in the form of both linear-linear and log-linear time-temperature curves (Figures 2a and 2b respectively). Note that even in the log-linear plot the data curve is not uniformly a straight line as predicted by Equation 1. The initial stages of each curve deviate from the ideal linear expression (Equation 1) due to contact resistance at the probe surface, transient behaviors within the heater probe itself, and other factors associated with specimen construction. The later portions of the curve will deviate as the conditions at the outer boundary of the specimen begin to come into play. In general, each individual run lasted for a total of six hours. The data analysis, however, is performed using only a sub-section of the total data chosen such that the curve most nearly approximates the ideal linear behavior expected. In the case of these tests, a typical data analysis involved the evaluation of data representing a period of approximately one hour as illustrated in Figure 2. A least-squares data fit is performed for the selected subsection of each data set to determine the slope of the linear relationship.

The average value of the effective thermal conductivity for the 12AWG 3-conductor cable bundle was 0.087 BTU/ft/hr/°F (0.15 W/m/°K). For the 8AWG single conductor cable bundle, an average thermal conductivity of 0.10 BTU/ft/hr/°F (0.18 W/m/°K) was measured. The experiments were repeated several times for each of the two bundles, and these values represent the average of all runs for each case. The maximum deviation in the measured conductivity from run-to-run was on the order of ±3% for each bundle (±5 in the third decimal place for the metric values cited above).

It should also be noted that these values are self-consistent. That is, as illustrated in Figure 1, the 12AWG cable is comprised of a larger fraction of plastic insulation materials as compared to the 8AWG single conductor cable which is dominated more heavily by the copper conductor. Hence, the 8AWG cable bundle should show a greater composite thermal conductivity because of the larger fraction of high-



(a) Data set plotted on a linear-linear time-temperature scale.



(b) Data set plotted on a log-linear time-temperature scale.

Figure 2: Data from a typical individual run of the 12AWG cable probe. Note indication of data sub-set used in actual determination of thermal conductivity.

conductivity copper. Hence, the variation between these two cable bundles does, in fact, reflect actual variations in the composition of the cable bundle medium.

Comparison to Previously Published Values

In a review of analytical efforts recently completed by SNL[3], two values of the assumed thermal conductivity of the cable mass region were cited in the public literature. Stolpe [3] used a value of 0.15 BTU/ft/hr/°F (0.26 W/m/°K). Engmann [4] used a value of 0.12 BTU/ft/hr/°F (0.21 W/m/°K). The exact basis for these values was not provided. (Note that in a third unpublished work McKelvey[4] used the thermal conductivity of the cable region as a free parameter in order to match experimental ACF values to those predicted by his thermal model. His final assumed value for cable mass thermal conductivity was 1.3 BTU/ft/hr/°F (2.3 W/m/°K) an order of magnitude greater than any of the other values cited here. However, McKelvey's computer coding of his thermal model included certain errors which contributed to this result. Hence, McKelvey's value is considered to be in error, and will not be discussed further here.)

Note that both of the values found in the SNL tests are considerably lower than those assumed by Stolpe and by Engmann. Recall that Fourier's Law states that the heat flux (rate of heat transfer per unit area, (q")) in steady state conduction is directly proportional to the temperature gradient (dT/dx), with thermal conductivity (k) being the proportionality constant:

$$q''_{cond} = -k \frac{dT}{dx}$$

Hence, the impact of a reduced thermal conductivity would be the relative "slowing" of heat transfer rates within the cable mass; or as an alternate view, a reduction in thermal conductivity would result in higher temperature gradients required to support a given level of heat transfer.

The primary effect of such a change would be to increase the hot-spot temperature for a given ampacity (heat load) in the cable bundle; or alternatively, to reduce the allowable ampacity for a given hot-spot temperature. This would apply to both the base line (unprotected) and clad (protected) conditions for a fire barrier protected cable

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[3]Ibid

[4]This unpublished work was performed for Thermal Sciences Inc. (TSI), the manufacturer of Thermo-Lag fire barrier materials and was reviewed by SNL as documented in a letter report of 10/29/93 under USNRC JCN J2018 Task Order 1.

system, and hence, the impact of conductivity changes on fire barrier ACF values is more difficult to assess. The reduced conductivity would also shift the location of the hot-spot within the cable mass. In general, because the upper surfaces are more effective at heat transfer (due to buoyancy induced convective enhancement), the location of the hot-spot would be expected to shift downward within the cable mass.

These effects were, in fact, noted during thermal model simulations performed by SNL as a part of its review of past analytical studies[5]. As a part of these efforts, SNL assembled a simple cable tray thermal model based largely on the earlier efforts identified during a literature review. Those aspects of each of the earlier models which were considered "best" were consolidated into an improved thermal model for the simulation of cable tray fire barrier ampacity effects.

In reviewing the past simulations, and in performing our own simulations, it was found that while estimation of the relative impact of a fire barrier on ampacity limits was relatively simple (i.e. calculation of the ratio of clad to unprotected ampacity), the prediction of absolute ampacity values was much more difficult. In our attempts to match certain experimental data on actual measured clad and unprotected cable tray ampacities, we found it necessary to reduce the assumed value of the cable region thermal conductivity from the values cited in the literature (0.12-0.15 BTU/ft/hr/°F) to a value of 0.08 BTU/ft/hr/°F (0.14 W/m/°K). Note that this value cited in the SNL calculations is quite similar to that found in the experiments for a 3-conductor 12AWG cable, namely, 0.087BTU/ft/hr/°F (0.15 W/m/°K). This is quite encouraging in light of the fact that the same type of cable was also used in the ampacity experiments being simulated, a 3-conductor 12AWG cable, although the exact composition of the cable insulation materials is unknown.

Summary

Tests were performed to measure the equivalent thermal conductivity for two different cable bundles. In the testing of a tightly packed 3-conductor, 12AWG, 600V, XLPE/CSPE cable a conductivity of 0.087 BTU/ft/hr/°F (0.15 W/m/°K) was measured. For a tightly packed single conductor, 8AWG, 600V, PVC/XLPE cable bundle, an average thermal conductivity of 0.10 BTU/ft/hr/°F (0.18 W/m/°K) was measured.

These values were compared to those used in previously published analytical studies of cable heat transfer behavior. In particular Stolpe [3] used a value of 0.15 BTU/ft/hr/°F (0.26 W/m/°K) and Engmann [4] used a value of 0.12 BTU/ft/hr/°F (0.21 W/m/°K). These values were assumed for cables nominally similar to the 3-conductor 12AWG cable bundle tested by SNL. The SNL determined

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[5]Op. Cit.

values are considerably lower than those assumed in these previous studies. As used in most simple cable tray thermal models, the net effect of a reduction in cable mass thermal conductivity would be a decrease in the predicted absolute ampacity limits for a given cable arrangement.

Finally, previous efforts at SNL included the use of a consolidated cable tray fire barrier thermal model to simulate the behavior in certain previously reported cable tray fire barrier ampacity derating tests. In these analyses it was found that using a thermal conductivity value of 0.08 BTU/ft/hr/°F (0.14 W/m/°K) provided the most satisfactory results when the comparison of analysis to experiments was based on matching of the actual absolute cable ampacity values rather than simply matching the relative impact of the fire barrier system. This finding agrees quite favorably with the results reported here for the 3-conductor 12AWG cable bundle which closely matches the cable types used in the actual cable tray ampacity experiments.

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Technical Evaluation of TUE Response
to Ampacity Derating Questions
Raised August 30, 1994

Enclosure 4

Technical Evaluation of TUE Response to Ampacity Derating Questions Raised August 30, 1994

A Letter Report to the USNRC

Draft Revision 0

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OVERVIEW

USNRC efforts to assess the adequacy of various Texas Utility Electric (TUE) submittals regarding the assessment of ampacity derating factors for it's Thermo-Lag cable fire barrier systems have supported by SNL since September 1993[1]. As of August, 1994, three technical questions remained unresolved. Each of these three questions was associated with uncertainty in the manner in which the TUE ampacity tests were being performed or in how the test results were being applied to specific plant installations. These three questions were communicated to TUE during a meeting between the USNRC and Texas Utilities Electric (TUE) held August 30, 1994. The three questions were related to:

Question 1: Use of the Sil Temp Blanket in Cable Tray Ampacity Testing
Question 2: Ampacity Derating for Untested Configurations
Question 3: Use of Different Conduit Specimens for Clad and Base Line ampacity Derating Tests

Since the August 1994 meeting TUE has provided additional information regarding these three questions. The TUE response was presented by the USNRC for SNL review in the form of (1) a letter from C.L. Terry of TUE to the USNRC Document Control Desk dated 11/18/94, and (2) a set of supplemental documents communicated to SNL by R. Jenkins of the USNRC under a cover letter dated 12/21/94. The supplemental documents are identified as "Attachments H and K of TUE document ER-ME-082, Revision 2 as well as a document entitled 'Heat Transfer Analysis.' "

The purpose of this report is to document SNL's review findings regarding this most recent TUE response. It was also requested by the USNRC that, as a part of this review, SNL should perform supplemental analyses, as appropriate, to "fill in" any perceived "gaps" in the TUE response. Such supplemental analyses were, in fact, performed by SNL and are documented in this report.

SUMMARY OF REVIEW CONCLUSIONS

Based on the TUE response to the USNRC RAI and on supplemental analyses performed by SNL as outlined below, SNL concludes that all of the technical questions raised by SNL to date regarding ampacity derating for TUE Thermo-Lag fire barrier systems have now been adequately addressed. In particular, SNL has performed supplemental analyses to assess the adequacy of the TUE untested configuration ampacity margins, and to assess the impact of potential variations in the properties of the tested conduits on ampacity derating factors. Based on these analyses, and on the information provided by TUE, SNL concludes that the large ampacity margin which is available at TUE is sufficient to bound the uncertainty associated with these issues. No additional questions regarding ampacity factors for TUE have been identified at this time. No further action regarding the TUE ampacity submittals is currently recommended by SNL.

[1]These earlier review findings are documented in previous SNL transmittals to the USNRC associated with this Task Order.

EVALUATION OF TUE RESPONSE TO SPECIFIC USNRC QUESTIONS

Question 1: Use of the Sil Temp Blanket

The first of the three unresolved questions was associated with the need to clarify how the Sil-Temp blanket material was utilized in the cable tray ampacity tests performed by TUE. The clarification provided by TUE in the 11/18/94 letter cited above fully resolves this uncertainty. It is now clear that the Sil-Temp blanket was not used in the cable tray base line tests, and that it was used in the cable tray clad tests. This represents the conservative combination of test configurations and appropriately reflects the actual use of the Sil-Temp blanket under TUE's fire barrier design practices. TUE's practice in the performance of cable tray ampacity tests was entirely appropriate. No unaddressed concerns remain regarding this particular question.

Question 2: Untested Configurations:

The ampacity testing performed by the utility has not encompassed all of the plant configurations. Hence the USNRC RAI identified the need to bound the uncertainty as to the extent to which the untested configurations might impose additional ampacity derating impact. TUE was requested to provide (1) an identification of the specific instances involving untested configurations, and (2) an analytical assessment to justify those untested configurations which are considered potentially most severe. TUE has now provided a response to this request, and SNL has reviewed this response.

The information provided by TUE does give sufficient detail regarding the physical identification and basic characteristics of those installations which fall into the category of untested configurations. Most of the modifications involved only very minor or very localized changes to the barrier installations procedures. Modifications of this type would not be expected to significantly impact ampacity derating factors. The untested configurations were categorized by TUE into 5 groups. SNL's evaluations regarding each of these groups are summarized in the following subsections.

Modifications at Raceway Transition Points

The modifications cited by TUE in this regards appear quite minor from the standpoint of ampacity derating. They apparently involve factors such as support obstructions, other local obstructions, radial bend coverage, wall proximity, and blockouts. The barrier modifications described by TUE which were needed to address these situations would not be expected to impact ampacity significantly due to their very localized nature. Such localized variances would be mitigated by lateral heat transfer to adjacent sections of the raceway with no significant impact on ampacity limits. While some localized effects might be expected, only a very small margin would be needed to bound the effects. The relatively large margin available at TUE would clearly be sufficient to bound these cases.

Modifications to the Application of the Stress Skin

Simple modifications to the outer stress skin installations such as those described by TUE would represent a very minor change from the standpoint of ampacity derating. Such changes

would have a very minimal effect on the overall heat transfer process. The primary impact would be introduced through thickness changes to the barrier material. However, as demonstrated by testing and analysis, the thickness of the barrier material is a secondary effect as compared to the mere presence of the barrier. Only a very minor ampacity derating impact would be expected due these modifications. Clearly, TUE's available ampacity margin will easily bound these installations.

Upgrade to Conduit Installations

Most of the conduit installation upgrades cited by TUE involve flexible conduit bends for which custom enclosures rather than pre-formed bends were installed. These variances are cited as extending no more than three linear feet along the length of a conduit. SNL agrees with TUE's assessment that these variances would not be an ampacity concern in this case. SNL bases this assessment on two factors. First, the short lengths involved would help to mitigate the localized heating effects to a large extent due to conduction of heat along the length of the conduits. Second, the fact that TUE's conduit installed cables can uniformly accept a 30% derate (as stated previously by TUE) would clearly bound these installations. Generally, conduits have shown derating impacts of 11% or less (based on testing and analysis). Hence, a significant margin remains for the conduit installations.

TUE also states that "there are instances where conduits are under a common enclosure. This occurs at conduit fittings and the common enclosure lengths are less than 3 feet." If the lengths of conduit involved were greater, then some basis for concern would remain. However, in this instance SNL agrees with TUE's assessment that the existing ampacity margin available to TUE (30% derate) would be more that adequate to bound these types of situations. The short distances involved would again mitigate local heating effects by conducting heat laterally along the length of the conduit through both the conduit itself and the copper conductors.

Finally, TUE discusses an installation in which a conduit barrier merges with a tray barrier. SNL had previously expressed concern over the extent to which the conduit ampacity might be affected by this configurations. (SNL concern over this configuration has focussed on the conduit, and only a very minor impact was expected for the cable tray in this situation. This conclusion is further substantiated by the TUE response which states that only 5% of the cable tray surface is affected.) Here again, SNL would agree with TUE's assessment of adequacy for this case. This is based on the conditions cited by TUE which state that only 10% of the conduit surface is affected by the merging of the fire barriers. Using this information an approximate supplemental analysis was performed by SNL (see Attachment 1). Based on this SNL analysis, the additional impact of this change could be expected to result in an additional ampacity derating penalty of approximately 9% or less over and above the worst-case TUE conduit testing value of 11% for a total derating factor of 20% or less. This derating still remains well within TUE's allowable margin of a 30% derate. Hence, no further concerns remain regarding this configuration.

In general, the additional derating penalty associated with the cladding of a conduit is relatively small. In the case of a conduit, there is already a significant reduction in ampacity due simply to the presence of the conduit as a thermal barrier (as reflected in the ampacity tables for cables in conduit). The additional reductions introduced by the installation of a fire

barrier system will be of secondary importance. This is reflected in the relatively modest conduit derating factors which have been found in testing. Hence, the large margin available at TUE would be adequate to ensure that these conduit installations are acceptable.

Modifications to Flexi-Blanket Installation

The flexi-blanket modifications cited by TUE are not of concern from an ampacity standpoint. These modifications typically involved obstructions which prevented a full additional layer from being installed on an item. No cases were cited in which additional layers of the material were added to an item. As long as no additional layers of insulation are added to an item, the standard test configurations would bound the installations. Obstructions such as those cited by TUE which intrude into a flexi-blanket envelope would also help to reduce cable operating temperatures because they would act as heat sinks. Therefore, one would expect that these installations would actually experience less severe ampacity derating than would the tested configurations. Use of the nominal test derating factors will provide conservative bounding conditions for these installations.

Raceways Under Common Enclosures

TUE cites certain instances in which two cable tray raceways are enclosed in a single common enclosure. For many of the cases cited the raceways run side-by-side. These situations would not be expected to fundamentally impact ampacity derating. From a heat transfer point of view, a pair of side-by-side trays would look essentially identical to a single wider cable tray because heat transfer from the sides of the tray is relatively unimportant to ampacity (as shown by testing and analysis). The standard test results would be expected to adequately bound side-by-side barrier configurations.

Of more significant potential concern are those situations in which two horizontal trays stacked one tray above another are co-located in a single enclosure. In this case, the basic heat transfer process is fundamentally altered. All of the cases of this type cited by TUE involve a power tray located above a control tray. Thus, while no additional heat load is added to the enclosure by the lower tray, the lower tray does restrict the flow of heat from the upper tray to the environment. The additional impact of this configuration on ampacity derating will be strongly dependent on a number of physical parameters (not all of which have been fully characterized by TUE).

TUE cited an analysis which purported to demonstrate that the increased surface area introduced on the sides of the larger box would more than compensate for the insulating effects of the second lower tray and the resulting reduction in heat transfer through the bottom of the box. SNL has reviewed the TUE analysis as it was presented in the supplemental documentation provided (see citation above, "Heat Transfer Analysis"). SNL finds this analysis to be poorly founded from a technical standpoint, and hence, inadequate. A full critique of the analysis is provided in Attachment 2 to this document. To summarize the findings of this critique, the TUE analysis provides an inadequate treatment for the heat transfer from the sides of the box and makes poorly founded assumptions regarding the temperature behavior of the box surfaces. This analysis should not be credited.

As an alternative, SNL has performed scoping analyses of a two-tray stacked configuration using a modified version of the cable tray ampacity analysis program FITCOND developed by SNL as a part of USNRC JCN J2018, Task Orders 1 and 2. These SNL analyses are described in detail in Attachment 3. It should be noted that the modified 2-tray model cannot be validated at this time due to a lack of experimental data. Hence, the primary modeling results (predictions of actual cable ampacities for a given situation) must be viewed with some skepticism. However, the results appear self-consistent with earlier modeling results, and appear to provide reasonable estimates of the relative ampacity derating impact. Hence, these analyses should provide a reasonable estimate of the additional derating impact which might result from these configurations. It should also be noted that SNL performed simulations using both best-estimate and conservative treatments of the inter-tray heat transfer behavior. Hence, while the results do contain uncertainty, the worst-case conditions should be bounded by these analyses.

Several cases were run to evaluate the effects of various parameters on the ampacity derating factors. One critical factor of importance is the cable tray-to-cable tray separation distance. The TUE analysis states that the minimum separation between its trays is 9". This is important because the additional derating impact increases significantly as this separation distance decreases. SNL performed simulations for trays as large as 36" wide, assuming a 9" spacing between trays. This included the simulation of lower tray cable depths of as much as 4" (fully loaded). The worst-case simulation (performed using the conservative heat transfer assumptions) predicted an additional derating penalty for a two-tray configuration of 6.3% as compared to a predicted single-tray configuration derating factor. Given the TUE experimentally determined ampacity derating factors (32%), and the available margin on cable tray cable ampacities (40%), even this additional ampacity penalty (for a total worst-case derating of about 38%) would still leave a significant ampacity margin. The best-estimate simulations generally predicted an additional ampacity penalty in the range of 2%-4% for configurations considered representative of the TUE installations. Based on these analyses, SNL concludes that the available TUE ampacity margin would be sufficient to bound the two-tray enclosure configurations as described by TUE.

Q3: Use of Different Conduit Specimens for Clad and Base Line Tests

The final question was associated with the fact that in testing TUE used different physical conduit specimens to perform it's base line and clad conduit ampacity tests. In particular, concern was expressed regarding the relative susceptibility of the tested specimens to inductive currents, and regarding the surface emissivity of the tested specimens. The test specimens were taken randomly from general TUE stocks, and TUE had not demonstrated that the clad and base line specimens tested were sufficiently similar to each other so as to not impact test results, nor had they demonstrated that the tested conduits bounded conduits installed in the plant. Hence, a certain level of uncertainty in the test results must be assumed.

If the conduit susceptibility to inductive currents varied between the clad and base line tests, then an impact on the final ampacity derating factors would result. In the case of emissivity, if the base line conduits tested did not bound the plant conduits, or if the clad and base line conduits had significantly different emissivities, the experimental ampacity derating factors may have been affected. These two questions are discussed in the following sections.

Inductive Heating Effects

TUEs response to Question 3 focussed entirely on this aspect of the question, the issue of inductive currents. The TUE response also considers only the question of what impact inductive heating has on test currents in general, and does not address the question of variations in behavior between the clad and base line tests. SNL has previously stated that, provided the behavior is consistent between the clad and base line tests, then the impact of inductive heating would be more conservative derating factors. This is consistent with TUE's conclusions as set forth in their response.

However, the issue cited by SNL on August 30, 1994 is that because TUE used different physical test specimens in the base line and clad tests, and because TUE cannot demonstrate that the properties of the two conduit specimens were identical, the test results may have been biased by differences in inductive heating behavior between the clad and base line tests. The impact of such a difference could be either conservative or non-conservative as follows:

> A conservative result would be obtained if the base line conduit were less susceptible to inductive currents than was the clad conduit. The presence of inductive currents generally reduces allowable ampacities. The more pronounced the inductive effect, the more current limits are reduced. Hence, this arrangement would impact (reduce) the base line current by a smaller fraction than it would the clad current. This would tend to increase the derating factor determined in such a test.
>
> A non-conservative result would be obtained if the base line conduit were more susceptible to inductive current that were the clad conduit. (The reasoning is essentially the same as above.)

The question not answered by TUE is "what is the upper bound on the actual ampacity derating factors which might be expected given that the conduit properties are unknown."

In an attempt to answer this question SNL sought information on how much variation might be experienced in inductive heating susceptibility between conduit specimens. No specific information was found which would directly answer this question for conduits in particular. However, SNL did find that only relatively minor variations are cited in the literature for different formulations of steel and iron at low-to-modest magnetic flux levels (see for example the CRC handbook of chemistry and physics). Hence, it is likely that the conduit samples tested by TUE were similar in this regards.

Of more significant interest, it was found that at relatively low magnetic flux levels, the susceptibility of a steel specimen to induced currents increases sharply with increasing magnetic field strength (ibid). This increasing susceptibility to induced currents would imply that as the rate of current flow in the cable increases, a non-linear increase in the inductive current problem would also be expected. This is, in fact, reflected in the TUE testing in that by far the most pronounced problems were noted for the larger, higher ampacity, power cables. This would imply that for a given specimen, the higher currents experienced in the base line tests would have increased susceptibility to inductive currents as compared to the susceptibilities in the clad tests where lower cable currents prevail. As noted above, this would represent a conservative effect in testing.

non

Based on these findings, SNL would conclude that any potential variation in conduit inductive current levels which might have been experienced by TUE in its tests would have contributed to an increased level of conservatism the TUE tests. SNL recommends that no further actions regarding this particular question be taken at the current time.

Variations in Conduit Surface Emissivity

As noted above, the question of conduit emissivity has two aspects. First, the conduits used in testing should bound the thermal properties of the conduits used in the plant. In this case, it would be most appropriate to ensure that high-emissivity conduits were tested so as to ensure that the base line currents were maximized. Secondly, it is important to ensure that the base line and clad conduits possesses similar properties. Variations between tests would impact the test results.

Note that with regard to the "variation between tests" aspect of this question, there is one aspect of the TUE installations which introduces this concern. In particular, the TUE Thermo-Lag conduit installations generally utilize pre-formed Thermo-Lag sections which are secured around the subject conduit. In installing these sections only intermittent contact will be made between the inner surface of the barrier and the conduit. Thus, an air gap will be created between the outer surface of the conduit and the inner surface of the barrier system. It is the presence of this gap which introduced this second point of concern. In particular, radiative heat transfer through this gap region will be important to the overall heat transfer process, and hence, the emissivity of the conduit remains an important parameter. If, alternatively, this air gap were filled with trowel grade material during installation, then the issue of variation between tests would not be relevant. With no air gap, the emissivity of the conduit is eliminated as an important parameter for the clad case, and only the first of the two issues identified above would remain.

TUE did not provide any direct assessment of this question. As an alternative, SNL has performed a supplemental analysis to assess the impact of conduit surface emissivity on ampacity derating as documented in Attachment 4. Based on this analysis SNL concludes that the worst-case impact would be to increase the ampacity derating factors as shown in Table 1. Even given these worst-case derating factors, the available ampacity margin at TUE (30%) is sufficient to bound these values. SNL recommends that no further actions regarding this question be taken at the current time.

Table 1: Worst-case derating factors for TUE tests based on "corrected" base line ampacities to account for potential variations in conduit surface emissivity.

Test Configuration:	Nominal Test ADF (%)	Worst-Case ADF (%)
3/C #10 AWG in 3/4" conduit	9.4	23.9
3/C #6 AWG in 2" conduit	6.6	21.5
4-1/C 750MCM in 5" conduit	10.7	25.0

Attachment 1

An Assessment of the Cable Tray-Conduit Merged Fire Barrier Installation as Described by TUE in TXX94287

In one case, TUE cites and instance in which the fire barrier systems of a cable tray and conduit merge due to the fact that the two items were too close together to provide independent barriers. The merger was described by TUE as occurring at an upper-outside corner of the cable tray (phone communication involving TUE, USNRC and SNL on 9/28/94). For this configuration, SNL expressed concerns that, depending on the extent of the contact, the conduit would suffer a larger than nominal ampacity impact. In the TUE response (TXX-94287) it is stated that the exposed surface area loss for the conduit was approximately 10%. Using this value an estimate of the additional ampacity impact can be made.

First, we must assume that there would be relatively little mutual heating effect between the cable tray and the conduit. This is justified based on the location of the conduit at the corner of the cable tray enclosure, and on the insulating nature of the fire barrier material itself. Using this assumption, one can estimate the impact on the conduit.

As a "first cut" it might be assumed that overall heat transfer would be reduced in direct proportion to the overall loss of surface area:

$$q_{new} = \frac{A_{new}}{A_{old}} q_{old} \tag{1}$$

or

$$\Delta q = (q_{old} - q_{new}) = \left(1 - \frac{A_{new}}{A_{old}}\right) q_{old} \tag{2}$$

This would, in effect, assume that the rate of heat transfer per unit of exposed surface area remained constant. This assumption is not exactly true because there would be some impact on the air flow patterns around the conduit which would adversely impact the convective heat transfer. Also, there would be some reduction in the "radiation view factor" from the conduit to the ambient which would introduce some additional loss of efficiency in the radiative heat transfer. As a conservative bound on these effects, it will be assumed that the loss of heat transfer for this case would be doubled from our base assumption above:

$$\Delta q' = 2\Delta q = 2\left(1 - \frac{A_{new}}{A_{old}}\right) q_{old} \tag{3}$$

or:

$$q_{new} = \left(2\frac{A_{new}}{A_{old}} - 1\right)q_{old} \tag{4}$$

Recall that the total rate of heat transfer from a cable system is proportional to the square of current flow in the cables. Hence, the ampacity correction factor (ACF) is given by the square root of the heat generation rate for a clad case (g_{clad}) to that for a base line case (g_{base}):

$$ACF = \left(\frac{g_{clad}}{g_{base}}\right)^{1/2} \tag{5}$$

In general, TUE has found that ampacity derating factors (ADF) for conduits are 11% or less. This corresponds to an ACF of 0.89 or greater (recall that ACF=1-ADF). If we assume a general ACF of 0.89, then the modified ACF for a merged conduit can be estimated based on the assumed loss of heat transfer due to loss of surface area (Equation 3). We must begin by finding (g_{clad}) as a fraction of (g_{base}):

$$g_{clad} = g_{base}(ACF)^2 \tag{6}$$

We now assume that the clad heat rejection capacity (g_{clad}) is reduced in accordance with Equation 4 above so that the modified clad heat rejection capacity is:

$$g'_{clad} = \left(2\frac{A_{new}}{A_{old}} - 1\right)g_{clad} = \left(2\frac{A_{new}}{A_{old}} - 1\right)(ACF)^2 g_{base} \tag{7}$$

We can now estimate the new ACF using the ratio of (g'_{clad}) to (g_{base}):

$$ACF' = \left(\frac{g'_{clad}}{g_{base}}\right)^{1/2} = \left[\frac{\left(2\frac{A_{new}}{A_{old}} - 1\right)(ACF)^2 g_{base}}{g_{base}}\right]^{1/2} \tag{8}$$

or more simply:

$$ACF' = \left[\left(2\frac{A_{new}}{A_{old}} - 1\right)(ACF)^2\right]^{1/2} \tag{9}$$

for this particular case:

$$ACF' = [\,(\,2(0.9)\ -1\,)(0.89)^2\,]^{1/2} = 0.80 \tag{10}$$

This indicates that as a <u>conservative</u> estimate of the impact of this situation on the conduit ampacity derating, an increase in derating from the nominal base value of 11% to on the order of 20% might be experienced. This derating still remains well within the TUE ampacity margin which could tolerate a 30% derating factor. Hence, it is concluded that this particular situation is acceptable based on TUE's available ampacity margin.

Attachment 2

An Assessment of the TUE Analysis Provided for SNL Review Under Cover Letter, R. Jenkins to S. Nowlen Dated December 21, 1994, Supplemental TUE Documentation Entitled "Heat Transfer Analysis - One Hour TSI Tray Enclosures"

Overview:

One of the untested TUE configurations which has previously been identified as of significant potential ampacity concern involved a situation in which two horizontal stacked cable trays, a power tray above a control cable tray, were housed in a common fire barrier enclosure. For this configuration, SNL had expressed concerns that the presence of the second lower tray in the enclosure would restrict the rate of heat transfer from the bottom of the upper tray, and hence, could lead to significantly greater ampacity derating factors than those which apply to a single cable tray enclosure.

The referenced TUE analysis was intended to demonstrate that this configuration is, in fact, bounded by a single-tray ampacity test. However, in reviewing this analysis SNL found that critical errors have been made by TUE. These errors involve poor choices for certain of the convective heat transfer correlations used, mistakes in the application of these convective heat transfer correlations, use of poorly founded assumptions regarding the temperature of the Thermo-Lag surfaces, and a fundamental over-simplification of the heat transfer problem. Based on these shortcoming, SNL recommends that the analysis results should not be credited.

Base Approach:

The TUE analyses are based on relatively simplistic application of convective and radiative heat transfer correlations to a particular geometry involving either one or two 24"x4" cable trays. The analysis is performed in two parts.

The first part of the analysis considers a single tray configuration protected by 1/2" of TSI materials (Thermo-Lag 330-1). This part of the analysis used an experiment performed on a similar tray by Omega Point Laboratory as a benchmark. The outer surface temperature of the fire barrier material is assumed to be uniform for the entire enclosure surface (the same for all surfaces, the top, bottom and sides). The value of this surface temperature is then adjusted until the predicted heat transfer rate from the outer surface of the fire barrier enclosure to the ambient environment matches the experimentally measured cable heat generation rate.

The second part of the analysis then considers the two-tray stack, single-enclosure configuration. The analysis assumes that the same TSI material surface temperature would apply, and proceeds to calculate the new total heat transfer rate assuming a

larger side surface area on the new box. The analysis conservatively ignores heat transfer from the bottom surface of the box for this case.

Based on this analysis, TUE concluded that the overall rate of heat transfer would be enhanced due to the increased side area. That is, TUE concluded that the increased side panel area would more than compensate for the reduced heat transfer through the bottom of the box.

Underlying Basis of the TUE Analysis:

The fundamental physical phenomena which underlies this analysis is that a vertical plate (the sides of the fire barrier box) will be much more effective at convective heat transfer per unit area than would a horizontal heated plate facing downward (the bottom of the box). This is a sound assumption in general, and hence, an increase in the area of the sides could, potentially, more than compensate for a reduction in heat transfer from the bottom of the box. The critical factor which must be considered is how much additional side panel area is added, and how much of this area really contributes significantly to the overall process of heat transfer.

Errors in the TUE Analysis:

1. Mis-Application of Horizontal Surface Heat Transfer Correlations: In the calculation of heat transfer coefficients for a horizontal surface (both the top and bottom of the box) TUE has applied inappropriate correlations and, even given the correlations used, has incorrectly calculated the characteristic length used in the correlations.

The TUE analysis is based on the use of very simple and common correlations for the rate of heat transfer from a horizontal plate. However, the correlations cited by TUE apply only to a rectangular plate with a given length and width. They are not intended to apply to a very long surface such as that of a cable tray (which is effectively infinite in length). (Note that the source of the TUE correlations was not specified, but such correlations are readily available from various texts. The exact same correlations were found by SNL in two independent texts; Holmann, 1976 and Pitts and Sissom, 1977.) The use of these correlations to simulate a cable tray geometry would lead to an over-estimation of the heat transfer coefficient.

Even given the correlations cited by TUE, the characteristic length scale (L) is improperly calculated. The correlations used by TUE should use a characteristic length which is the average of the length and width of the flat plat (clearly a problem for a semi-infinite plate). The TUE analysis has assumed a width of 24", the width of the cable tray, but has assumed a length of just 12". Presumably, because the analysis is performed on the basis of heat transfer per foot of cable tray, one foot was assumed as the length. This represents a fundamental mis-application of the correlations, and leads to a significant over-estimation of the value of the heat transfer coefficient for both the upper and lower surfaces.

In reality, the cable trays are 24" wide and very long (for all intents, infinite in length). This configuration leads to very different air flow patterns over the tray, and hence, requires the use of very different heat transfer correlations. For a very long

surface, a 2-dimensional flow pattern would exist whereas for a true rectangular plate, a 3-dimensional flow pattern would prevail.

More appropriate correlations for cable tray geometries are available and should be used. The assumption by TUE that the upper surface heat transfer can be characterized by a rectangular plate 12"x24" distorts the analysis significantly. The misapplication of these correlations is considered a fundamental and fatal flaw.

2. Uniformity of the Surface Temperature: The TUE analysis assumes that the entire surface of the box would be at a single temperature (sides, top and bottom). This assumption is not an accurate reflection of the actual conditions. Each of the surfaces of the enclosure is subject to different levels of heat transfer, and hence, to different surface temperatures. This has been demonstrated by both testing and analysis. To assume that all surfaces are at the same temperature fails to recognize this fact, and in effect, treats all of the surfaces as having equal merit in the overall heat transfer process. This is clearly inconsistent with reality. For example, the upper surface of the enclosure accounts for a larger fraction of the overall heat transfer rate than does the lower surface due to the fact that buoyancy enhanced convection prevails for an upward facing heated plate. Of most critical importance, by assuming a uniform outer surface temperature, the temperature of the side panels may be over-estimated, and hence, their contribution to the overall heat transfer process may also be over-estimated.

It should also be noted that, for the case of the two-tray configuration, the temperature of the side panels themselves would vary over the height of the panel. In particular, those areas adjacent to the lower cable tray would be much cooler, and hence contribute less to heat transfer, than would the areas covering the gap between the two trays and that adjacent to the upper tray. This would follow when one considers the fact that the lower tray is not generating any heat, and the side rails of the lower tray would isolate the adjacent sections of the side panels from any significant interaction with the upper tray radiative and convective heat transfer behavior.

TUE's treatment of the side panels as being uniformly effective fails to reflect the actual conditions which would prevail and is non-conservative.

3. Case to Case Surface Temperature Behavior: TUE's analysis also assumes that the temperature of the enclosure surfaces would not change from one case to the next. This assumptions reflects an over-simplification of the problem, and could lead to erroneous conclusions. In general, if all of the other factors were properly handled (in particular, the variation of surface temperature for the various surfaces), then this assumption would be of only secondary concern. However, given the other concerns related to the TUE analysis, this error becomes more significant.

Conclusions:

Due to the manner in which the TUE analysis was executed, it is recommended that it be given no credit as demonstrating that the TUE two-tray, single-box configuration is acceptable. While the TUE conclusion may, in fact, be correct for at least some configurations, this analysis was performed poorly and should not be credited as supporting TUE's conclusion. (Note that SNL has provided an alternate analysis

which provides a more reasonable estimate of the impact of such a configuration on cable ampacity limits.)

Attachment 3:

SNL Analysis of a Two-Tray, Single Enclosure Fire Barrier System

Introduction:

The analyses described here were performed in order to estimate the additional ampacity derating impact which might result for a configuration in which two horizontal trays in a vertical stack are housed in a single common fire barrier enclosure. The analyses presented here must be considered as approximate only in that no experimental data currently exists against which the modelling results can be compared. However, it should also be noted that SNL has performed the analyses using both a best-estimate version of the model and a version considered inherently conservative. Results for both versions are compared.

Unique Aspects of this Model:

The analytical model used to perform these analyses was incorporated into a modified version of the Fortran computer code FITCOND. The original FITCOND model was developed by SNL as a part of USNRC JCN J2018, Task Orders 1 and 2. The base model provides predictions of the heat transfer behavior of a single cable tray either in open air or when enclosed in a fire barrier system. The modified model described here uses all of the original features, and simply adds an additional intermediate thermal layer comprised of the inter-tray air gap and the lower cable tray. The modified model has several unique aspects which should be noted. In particular, the following assumptions are made:

- Only the upper of the two cable trays generates any heat. The lower tray is assumed to house only unpowered control and instrumentation cables. (This is consistent with the TUE configurations.)

- Both cable trays are assumed to house a solid and uniform mass of cables of a user specified thickness (each of the two cable masses may have different thicknesses). An equivalent thermal conductivity for this composite mass is assumed, and both trays use the same assumed value for this parameter.

- The treatment of the problem is fundamentally one-dimensional (1D). A quasi-2D treatment is provided in that several geometric factors associated with the space between the cable trays are used to estimate the rate of heat transfer through the sides of the box. Nonetheless, the treatment remains fundamentally 1D.

- In treating the heat transfer through the sides of the box, only that area which falls between the two cable trays is considered active. That is, heat transfer through the sides of both cable trays is assumed zero, and no convective or radiative interaction is assumed to take place with those portions of the side panels which are adjacent to both the upper and lower cable tray side rails. This assumption is conservative, but is also considered grossly accurate given the nature of the fire barrier

enclosure. That is, the actual involvement of these parts of the side panel are largely blocked-out by the cable trays themselves, and hence, little heat would be expected to flow through these regions (this would, in reality, be reflected by variations in the surface temperature over the height of the side panels as compared to a uniform temperature).

- The heat transfer in the space between the two cable trays has been treated in two ways. Conservative calculations are performed which ignore all convective and conductive interactions through this intermediate air gap. For these calculations it is assumed that only radiative exchange takes place. Best-estimate calculations are also performed which incorporate non-ideal convection coefficients in the calculations in addition to radiative transport. (This issue is discussed further below.)

- In the utilization of geometric factors in the model, certain assumptions are made regarding the physical dimensions of a typical cable tray. Namely, a typical open ladder style cable tray is assumed to have a total side rail height of 5.25". Of this total height, the upper 4.25" is assumed to be available for the installation of cables (with a practical available depth of fill of 4"). The remaining 1" is assumed to be taken up by the rungs of the cable tray. Hence, a gap of 1" is uniformly assumed to exist between the bottom of the cables and the bottom of the cable tray side rails. Further, the width of the horizontal stiffening flange on the cable tray side rail is assumed to be 1" (hence, the side rails are assumed to be "C" shaped sections 5.25" tall and 1" wide). Finally, the width of the cable tray is assumed to be from the inside of one side rail to the inside of the second side rail. These factors play an important role in the calculation of the 'upper cable-to-barrier side panel' and 'upper cable-to-lower cable' radiation view factors. These assumptions are consistent with SNL experience with typical cable tray configurations.

- TUE's use of the SIL-TEMP blanket has been neglected in the SNL analyses (including both the base single-tray and the two-tray simulations). This is a conservative approach to the two tray problem. The use of the SIL-TEMP blanket by TUE (both in practice and in its testing program) leads to more severe ampacity derating factors for its single-tray barrier systems. This effect is not accounted for in the SNL simulations. However, the question which SNL has attempted to address is the <u>relative</u> impact of the two-tray versus single-tray configurations. If the thermal model were to include the Sil-Temp blanket as an additional thermal barrier for both the single and two-tray configurations, the predicted <u>relative</u> impact of the two-tray configuration over and above that of the single tray system would be less severe, even though the <u>absolute value</u> of the predicted derating factors would increase in both cases.

As noted above, all of the original features of FITCOND were retained in the current model. This included the treatment of heat transfer within the upper tray (the heat generating tray), the treatment of the upper air gap and top panels of the fire barrier system, and the treatment of the lowest air gap and heat transfer through the bottom panel of the fire barrier system. The modifications implemented for the purposed of this analysis incorporate an additional thermal layer inserted between the lower surface of the upper tray, and the upper surface of the lower air gap. The geometry modeled in the modified model is illustrated in Figure 1.

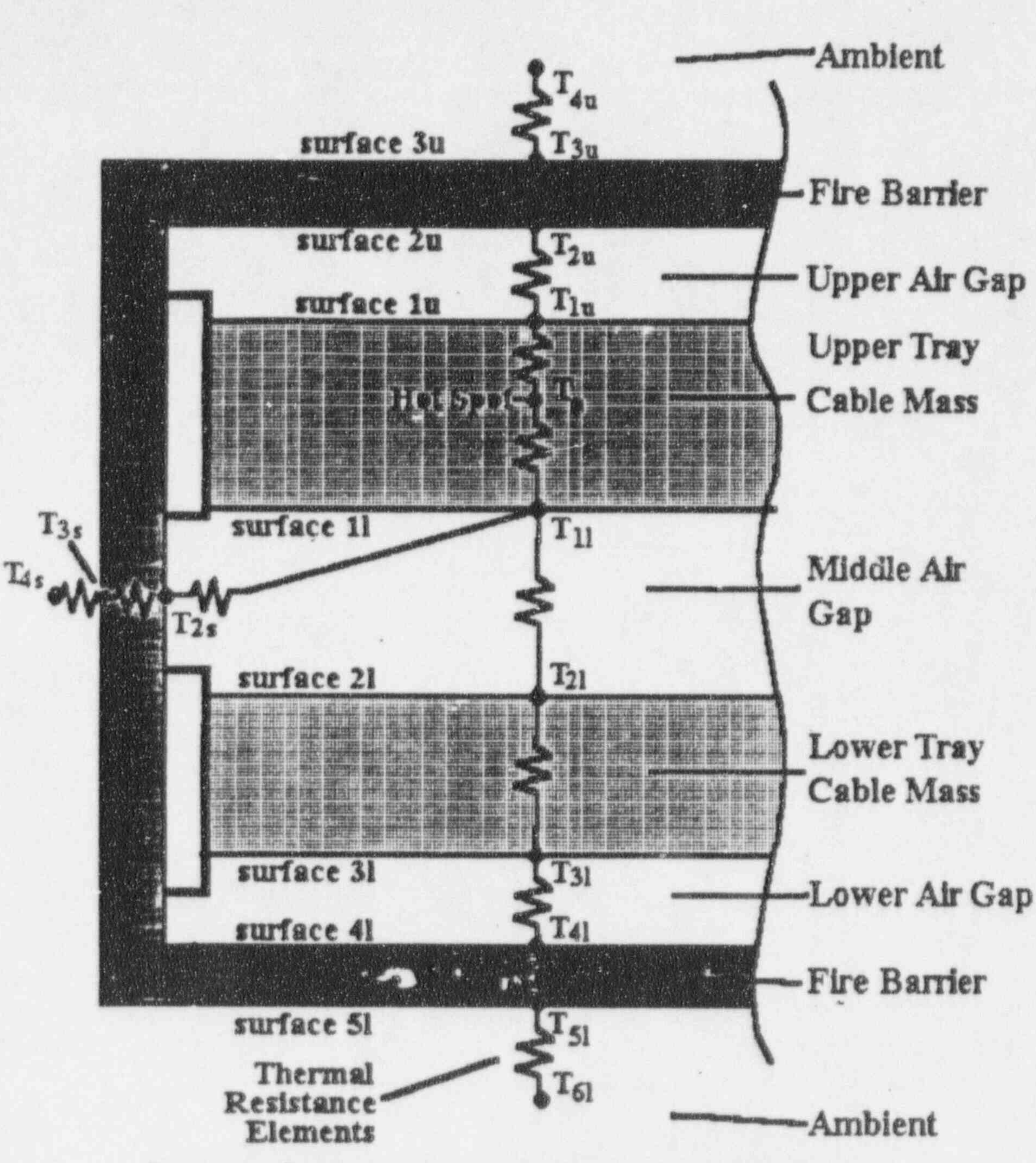

Figure 1: General geometry of the two-tray thermal model.

<u>Heat Transfer To and Through the Lower Cable Tray</u>

In modeling the heat transfer from the upper tray to the lower tray, two modes of heat transfer are possible; namely, radiative and convective. A discussion of the convective heat transfer treatment will be deferred until later (see below) due to the complex nature of this problem.

In modeling the intermediate thermal layer, it is assumed that the lower surface of the upper tray cable mass interacts radiatively with upper surface of the lower tray cable mass. Geometric parameters input by the user (height of the gap between trays, the width of the

cable tray, and the thickness of the lower tray cable mass) are utilized in order to calculate a radiative view factor between these surfaces. (Note the assumptions related to cable tray physical dimensions cited above play an important role in the calculation of radiation view factors.) Radiative exchange is then assumed to occur via the commonly cited radiative exchange relationship:

$$q_{r,1-2} = F_{1-2}A_2\,(T_1^4 - T_2^4)\left(\frac{1}{\frac{1}{\epsilon_1} + \frac{1}{\epsilon_2} - 1}\right) \tag{1}$$

As noted above, the model is fundamentally 1D, and so, all heat fluxes are treated on the basis of a heat flux per unit area of the cable tray. Hence, it is necessary to express this relationship in these same terms as well. This requires that the equation be divided by the source area:

$$q''_{r,1-2} = F_{1-2}\frac{A_2}{A_1}\,(T_1^4 - T_2^4)\left(\frac{1}{\frac{1}{\epsilon_1} + \frac{1}{\epsilon_2} - 1}\right) \tag{2}$$

For this particular case, the source and target are of the exact same area, and hence, the area ration reduces to unity. (As noted below, this does not hold true when the side panels are considered.)

Once the heat has been delivered to the upper surface of the lower tray cable mass, it is a simple process to calculate the temperature drop due to conduction through the lower tray cable mass:

$$\Delta T_{tray2} = \frac{t_{tray2}}{K_{cable}}\,q'' \tag{3}$$

were (t_{tray2}) is the thickness of the cables in the lower tray, and (K_{cable}) is the thermal conductivity of the cable mass.

From this point, the treatment of the final air gap (that between the lower tray and the bottom fire barrier panel), the bottom fire barrier panel, and heat transfer to the ambient is identical to that provided in the original version of FITCOND.

Heat Transfer To and Through the Side Panels:

Also simulated is the exchange of heat between the upper tray cable mass and the exposed portions of the fire barrier side panels. As with the discussion immediately above, a discussion of the convective heat exchange will be deferred (see discussion below).

The lower surface of the upper tray cable mass is assumed to interact radiatively with the inner surface of the fire barrier side panels. However, only that part of the side panel which is located between the two cable trays is assumed to take part in this exchange. The other

parts of the side panel, those parts adjacent to the upper and lower tray side rails, are assumed to be physically blocked from participation. Again, physical dimensions input by the user are used to calculate the radiation view factor for this exchange. In practice, there are two side panel surfaces so the view factor to one surface is calculated, and this value then doubled to reflect the two surfaces. Equations 1 and 2 above also apply to this exchange as well. However, in the case of the side panels, the source and target areas do not match. Instead, the user input geometry factors are used to calculate the area ratio as the ratio of the distance between trays to the width of the cable tray:

$$\frac{A_1}{A_2} = \frac{H_{gap}}{W_{tray}} \tag{4}$$

The heat which arrives as the inner surface of the side panel is then conducted through the side panel and the corresponding temperature drop is calculated in accordance with:

$$\Delta T_{barrier} = \frac{t_{barrier}}{K_{barrier}} q'' \tag{5}$$

were ($t_{barrier}$) is the thickness of the panel, and ($K_{barrier}$) is the thermal conductivity of the fire barrier material. The heat is then transferred to the ambient environment via both radiation and convection. For convection a common correlation for free convection from a vertical plate is used (Holmann, 1976):

$$h_{conv} = \frac{K_{air}}{H_{box}} 0.59 \ (Ra)^{0.25} \qquad (Ra < 10^9) \tag{6}$$

for laminar conditions, or:

$$h_{conv} = \frac{K_{air}}{H_{box}} 0.13 \ (Ra)^{0.33} \qquad (Ra \geq 10^9) \tag{7}$$

for turbulent conditions, where (K_{air}) is the thermal conductivity of air, (H_{box}) is the full external height of the fire enclosure box, and (Ra) is the Rayleigh number, a non-dimensional group. For calculation of the actual heat flux per unit area of cable tray, we must again normalize using the ratio of the side panel area to that of the cable tray:

$$q''_{conv} = h_{conv}\frac{A_2}{A_1}(T_{panel} - T_{ambient}) \tag{8}$$

Again, in this case, the active side panel area is considered to be only that in the gap region between the two trays consistent with Equation 4 above. Radiative heat transfer is handled in a similar manner to that of the upper and lower panels. However, again, only that portion of the side panel which is adjacent to the gap between cable trays is considered active in the radiative exchange with the ambient.

Treatment of Convection in the Mid-Gap Region

The treatment of convective heat transfer in the mid-gap region (the region between the two cable trays, presents a very difficult problem. The situation which must be simulated is a roughly rectangular enclosure of, effectively, infinite length which is heated from above and cooled on both sides and on the bottom. This problem, to our knowledge, has never been addressed experimentally, and hence, no correlations for this behavior could be identified in the current literature. This required that alternative correlations be applied which are not ideally suited to this geometry.

Note that due to the non-ideal nature of the correlations applied and due to the uncertainty associated with the application of these correlations to this problem, SNL has performed two sets of calculations for each of the parameter sets investigated. In one set, the convection relationships for the mid-gap region as described here were fully active. In the second set, the convection in the mid-gap region was neglected (set to zero) and only the radiative exchange as described above was modeled. This second set of calculations is intended to provide a conservative bound on the calculations in that it neglects a known heat transfer mechanism which would be active but which can be only poorly characterized.

The fundamental problem associated with convection in the mid-gap region arises from the fact that the space is heated from above and that cooling takes place along the sides of the region. A similar geometry which has been considered is natural convection in a closed enclosure heated from above and cooled from below but with insulated sides. In this case convection currents are negligible because the temperature distributions results in a stable buoyancy condition. No significant circulation takes place, and hence, heat transfer is by conduction through the air only (this is the treatment provided for the lower gap region for example). However, in the present case, cooling actions at the sides of the enclosure will disrupt this stability. In particular, air heated along the top of the space will be cooled along the sides of the space. This cooling will cause a downward buoyancy force near the sides, and hence, will induce an internal closed cell circulation flow as illustrated in Figure 2. This convective flow will act to enhance the rates of convective heat transfer significantly.

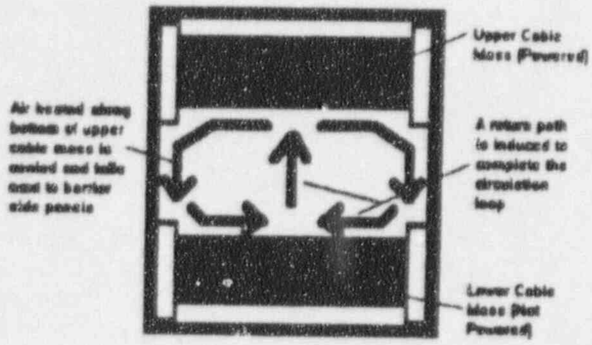

Figure 2: General pattern of convective air circulation expected for the two-tray fire barrier configuration.

In modeling this situation, SNL has applied flat plate correlations which are intended for use in the simulation of unconfined plates. These correlations are poorly suited to this situation due to differences in the nature of the convective flow paths which would be established in the open air versus those in the enclosed mid-gap region. The open air flow paths are illustrated in Figure 3.

The same correlation is used to estimate the coefficient of convective heat transfer from the lower surface of the upper cable mass to the air, and from the air to the upper surface of the lower cable mass (typical heat transfer correlations treat a downward facing hot plate and an upward facing cold plate using the same correlations). This correlation was taken from Incropera and DeWitt (1990):

$$h_{conv} = \frac{K_{air}}{(W_{tray} / 2)} \; 0.27 \; (Ra)^{0.25}$$

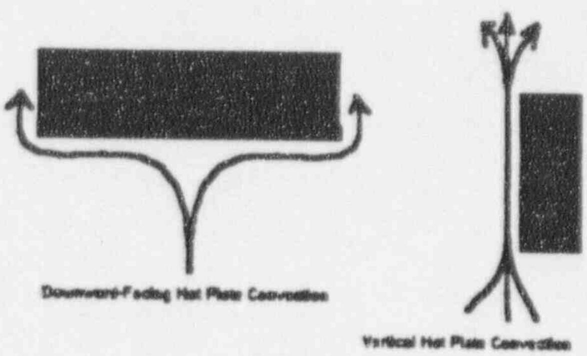

Figure 3: Air flow patterns for open plate convection.

where (W_{tray}) is the width of the cable tray. This is also the same correlation used in the original version of FITCOND, and in this modified version as well, for simulating convective heat transfer from the bottom of the fire barrier enclosure to the ambient.

For heat transfer from the air to the fire barrier side panels, Equation 6 above was used except in that the distance between the two cable trays (H_{gap}) was substituted for the total external box height (H_{box}) as the characteristic length scale. In the case of the side panels, the total heat flux was again normalized to heat flux per unit area of cable tray using an area ratio consistent with Equations 8 and 4 above.

<u>Model input Parameters:</u>

Using the thermal model described above, SNL performed several simulations in which a number of physical parameters were varied. These parameters were varied over a range which is expected to encompass the TUE applications based on the information provided by TUE and on SNL's knowledge of both the TUE ampacity derating program and the fire barrier fire endurance test programs. This was necessitated in that TUE has not provided concise descriptions of all aspects of the two-tray fire barrier enclosures. (Note that the need for such explicit information had not been anticipated, and hence, no requests for such extensive and explicit information were made to TUE.)

The parameters which were varied in an attempt to bound TUE's applications included the following:

- Cable Tray Width: Widths from 12" to 36" were evaluated. In general, for a given tray-to-tray spacing, wider trays will suffer a greater ampacity penalty.

- Thickness of the Cable Masses: The thickness of both the upper and lower cable masses will impact the ampacity calculations. In terms of the relative derating impact, the thickness of the lower, unpowered cable mass will be most significant. SNL has investigated lower tray cable mass thicknesses from 1" to 4". The upper (powered) cable mass thickness was typically set to 3" (about the same thickness as the standard ampacity derating test tray specified in IEEE P848 drafts).

- Tray-to-Tray Spacing: TUE states in its analysis that the minimum tray-to-tray spacing encountered in two-tray enclosures is 9". In general, a closer spacing will lead to a greater ampacity impact. SNL has investigated tray-to-tray spacings ranging from 1" to 12".

A number of parameters were held constant through all of the simulations. These include:

K_{cable}= 0.09 BTU/hr-ft^2-°F (based on SNL testing)
$K_{barrier}$= 0.122 BTU/hr-ft^2-°F (based on TSI data)
$t_{barrier}$= 0.5" (1/2" Thermo-Lag barrier thickness assumed throughout)
$T_{ho-spot}$= 90°C (cable hot spot temperature)
$T_{ambient}$= 50°C (consistent with TUE analyses)
ε_{cable}= 0.8 (cable emissivity)
$\varepsilon_{barrier}$= 0.9 (barrier emissivity)

$x_{low\text{-}gap}$= 1.0" (gap to bottom barrier panel, see tray geometry discussion above)
$x_{high\text{-}gap}$= $4.25\text{-}x_{top\text{-}cables}$ (gap to top barrier panel, see tray geometry discussion above)
$x_{mid\text{-}gap}$= $1+H_{gap}+(4.25\text{-}x_{low\text{-}cables})$ (mid-gap thickness, cables-to-cables)

Base-Case Modeling Results:

In considering the SNL analysis results presented here, it is recommended that the primary basis for comparison be the relative change in ampacity derating predicted for a two-tray enclosure as compared to an equivalent single tray enclosure. That is, the SNL results should be primarily viewed in terms of relative changes rather than as accurate estimates of absolute derating factors for a given configuration. This is recommended for two reasons. First, only limited validation of the SNL simulation models has been possible to date. Second, the SNL simulations have neglected the effects of the Sil-Temp blanket used by TUE in it fire barrier systems for wider cable trays. Based on this second factor, SNL would expect that the thermal model results would yield lower absolute derating factor predictions than those measured in TUE's testing.

The "base cases" involve the simulation of single tray fire barrier enclosures which were calculated using the original version of FITCOND. This included a calculation of both the un-protected thermal power density and the power density assuming a normal single tray Thermo-Lag enclosure. Based on these calculations, the nominal single tray ampacity derating factors predicted by the SNL model for various depths of cable fill are given in Table 1.

Table 1: Nominal ampacity derating factors for a single tray, 1/2" Thermo-Lag fire barrier system predicted by the SNL thermal model FITCOND.

Cable Depth of Fill (in.)	Ampacity Derating Factor (%)	Ampacity Correction Factor
1	36.2	0.638
2	30.5	0.695
3	26.2	0.738
4	22.1	0.779

It is these values which form the basis for comparison to the balance of the simulation results presented here. The remainder of the SNL simulations described here were performed using the modified version of the FITCOND program (TWOTRAY) with convection in the mid-gap region either active (best-estimate) or inactive (conservative).

As noted above, SNL expected its thermal model to under-predict the ampacity derating factors as compared to the TUE test results because SNL's thermal model, as exercised here, neglects the impact of the Sil-Temp blanket. This was in fact reflected in these results. TUE measured an ampacity derating factor of 31.5% for an IEEE P848 standard cable tray. This test would be most closely approximated in the current simulations by the 3" depth of fill case above for which SNL predicted a derating factor of approximately 26%. Previous simulations using a version of FITCOND which did account for the Sil-Temp blanket had resulted in a predicted ampacity derating factor of 31% for the single tray configuration[2]. Hence, the results appear self-consistent, and appear to match quite well the single tray experimental results, at least in regards to the ampacity derating factor predictions.

(Note that the increasing derating factors for a smaller depth of fill arise from two factors. First, the upper air gap (between the cable and the barrier top panel) becomes thicker as the depth of fill becomes smaller. This increases the thermal resistance across the upper air gap. Second, as the depth of fill increases, the thermal resistance between the cable hot spot (near the center of the cable mass) and the surface of the cable mass also increases. This implies that a large ampacity penalty is already imposed due to depth of fill, and hence, the <u>relative</u> impact of a fire barrier is less significant for greater depths of fill. Also note the these results are independent of tray width because of the assumption of true 1-dimensional behavior for these simulations.)

<u>Worst-Case Modeling Results:</u>

The single worst-case change in the estimated derating factor calculated in the SNL simulations which is potentially applicable to the TUE configurations involved a pair of 36" wide trays with a 3" depth of fill in the upper tray, and a 4" depth of fill in the lower tray. The tray-to-tray spacing was assumed to be 9", the value cited as the minimum separation distance by TUE. For this case, an ampacity derating factor of 32.5% was calculated as compared to the base single tray value of 26.2%. **Hence, it is estimated that the worst case two-tray configurations at TUE might result in an additional derating penalty of 6.3% on top of the nominal derating associated with an equivalent single tray enclosure. This <u>change</u> should bound that which might be experienced for all of the TUE two-tray configurations as they are currently understood by SNL.**

It should be noted that changing the depth of fill in the upper tray had little impact on the <u>change</u> in ampacity derating given that other factors remained the same. In fact, for this situation, a thicker depth of fill in the upper tray led to a slightly greater <u>change</u> in derating factors.

<u>Exploration of Parametric Effects:</u>

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[2]See SNL draft letter report to the USNRC, October 21, 1994, Fire Barrier System Cable Ampacity Derating, A Review of Experimental and Analytical Studies", JCN J2018, Task Order 2.

As a part of its simulations SNL explored how changes in various of the input parameters would impact the ampacity penalty for the two-tray configuration as discussed above. This section provides a discussion of these insights.

One of the factors not fully characterized for the TUE applications is the range of cable tray widths for the two-tray enclosures. Figure 4 illustrates the impact of this parameter on the ampacity derating factor for a fixed cable tray-to-cable tray separation distance (gap height). All of the simulations shown assume a 3" depth of fill in the upper tray, the approximate thickness of the cable mass in a standard test tray. These cases also assume a 9" tray-to-tray gap which is cited by TUE as the minimum tray separation distance. The results are illustrated for two depths of fill in the second lower tray, namely, 1" and 2". Note that as the cable tray width increases, the predicted derating factor also increases. This effect is due to the fact that as the tray becomes wider for a fixed gap height, the ratio of the gap height to tray width decreases. As illustrated in the analytical development above, the heat transfer through the side panels must be normalized by this ratio (see Equation 4, e.g.). Thus, the relative importance of the side panels is lowered as the tray width increases.

A similar impact is noted when changes in gap height are considered for a fixed tray width. Figure 5 illustrates this effect for a 24" wide tray with a 3" depth of fill in the upper tray and a 1" depth of fill in the lower tray. Note that as gap height decreases, the derating factor increases. The explanation is identical to that presented immediately above. In this plot there is, in fact, one case in which the gap width is sufficiently large that the predicted two-tray derating factor is lower than the nominal single tray derating factor predicted for this case (the 12" gap case where convection is included in the calculation). This is, in effect, what the TUE analysis predicted would occur for all cases. Clearly the SNL calculations do not agree with TUE's conclusion in this regards.

One final factor not characterized for the TUE applications is the thickness of the lower tray cable mass. As this mass increases in thickness, a greater insulating effect is realized, and hence, a greater ampacity derating impact would be expected. This is reflected in Figure 6 which illustrates the impact of the thickness of the lower cable tray cable mass on the resulting derating factor for a pair of 24" wide cable trays. For these cases it was assumed that the upper cable mass is 3" thick, and the tray-to-tray separation was 9".

Conclusions:

Based on the analyses performed by SNL, it is estimated that the worst case ampacity derating impact associated with the TUE two-tray enclosures would be to impose an additional derating penalty of 6.3% above that normally associated with a similar single-tray barrier configuration. This assumes that the maximum tray width applicable to the TUE two-tray enclosures is 36", and that the minimum tray-to-tray separation distance is 9". This worst-case estimate is based on simulations using conservative modeling assumptions. Best-estimate simulations, which remove certain of the modeling conservatisms, predict a somewhat less severe impact, generally ranging from 2%-4% additional ampacity derating for the configurations as described by TUE.

It should be noted that these estimates represent only the worst-case conditions for a range of the input parameters considered representative of the TUE configurations. Wider variation in

these parameters would be expected for the industry as a whole. Hence, these estimates should not be viewed as appropriate for application to the nuclear industry in general. Other plants and other configurations must be examined individually.

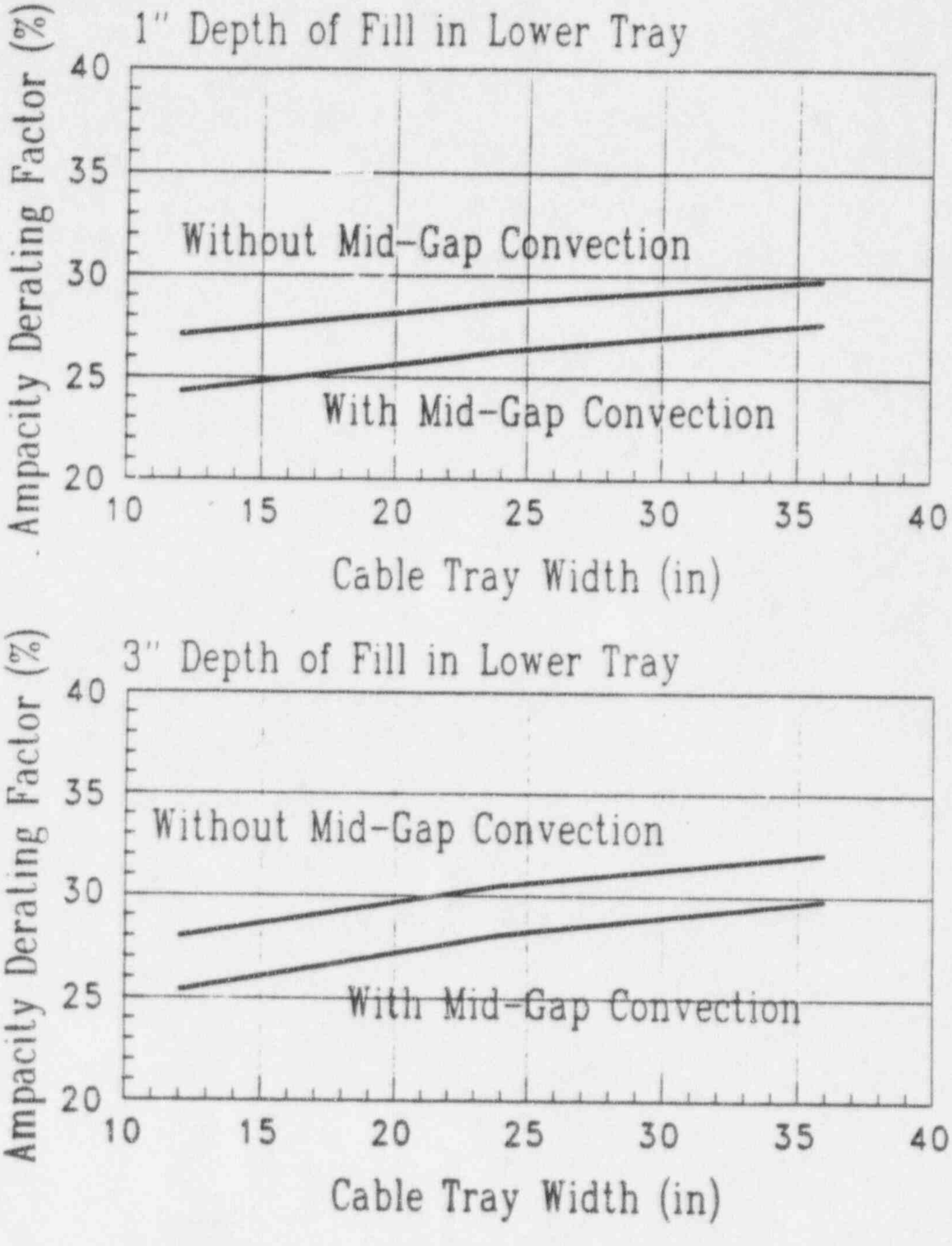

Figure 4: Effect of tray width on ampacity derating factors for a given tray-to-tray spacing.

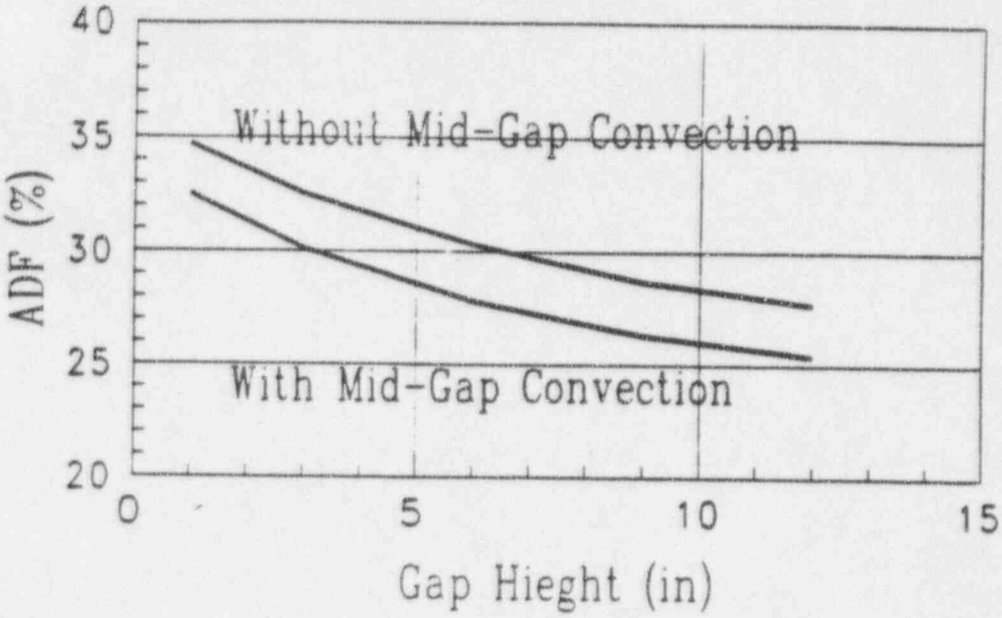

Figure 5: Effect of tray-to-tray separation distance on derating factor.

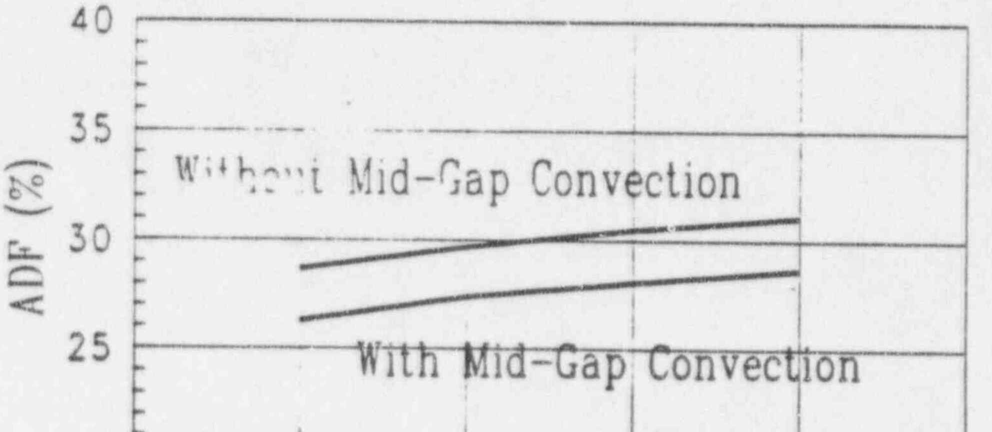

Figure 6: Effect of lower tray cable mass thickness on ampacity derating factor.

Attachment 4:

SNL Analysis of Conduit Surface Emissivity Effects

Overview

The issue of conduit emissivity has two aspects as identified in the body of this report. First is the question of conduit emissivity effects on the measurement of base line cable ampacity factors. The second is the question of how variations in this property between clad and base line tests might impact results. This attachment documents analyses performed by SNL to assess the uncertainty associated with these effects, and to assess the impact of that uncertainty on the TUE conduit ampacity measurements.

General Approach to the Worst-Case Analysis

In practice, both of the two emissivity effects identified above can be bounded using a single analysis. In this analysis, it is assumed, by pure chance, that the TUE tests were performed using the most favorable combination of conduit emissivity values in the clad versus base line tests. That is, it is assumed that the TUE tests were conducted in a manner such that the base line ampacity was minimized, and the clad ampacity was maximized. This would imply that the emissivity of the base line conduit was very low, and the emissivity of the clad conduit was very high.

Two approaches to bounding the impact of this effect on the test results can be taken. In each approach, the intent is to "correct" one of the two ampacity measurements to reflect the measurement which would have resulted had an equivalent conduit been used in both tests. That is, one approach would be to allow the measured base line test ampacities to stand, and to then "correct" the clad ampacities to reflect those which might be measured for a similar low emissivity conduit. This would involve a reduction in the clad ampacity as compared to the measured value, and a re-calculation of the derating factor. The second approach would be to allow the measured clad ampacities to stand and to then correct the base line ampacities to reflect those which might have been measured for a high emissivity conduit.

The second of these two approaches is the more conservative, and that which is taken here. That is calculations are performed to "correct" the base line ampacities, and the worst-case derating factors are then re-calculated. This approach is the most conservative for two reasons:

- Radiative behavior at the conduit surface is less important in the clad case than it is in the base line case. This is because for the clad case, in addition to radiation and convection/conduction through the air gap between the conduit and the barrier, a supplemental mechanism for heat transfer will exist

through the intermittent contact which will inevitably exist between the two surfaces. Depending on the extent of contact which does exist, this could be a very significant contributor to overall heat transfer rates. This reduces the relative importance of radiation, and hence surface emissivity, for this case.

- In general, the greater the ampacity "penalty" which has already been paid for the base line test case (from such factors as cable loading density, and conduit presence), the less significant is the relative additional penalty paid for the fire barrier system. A conduit with a low emissivity would impose a larger ampacity "penalty" than would a high emissivity conduit, and hence, the relative impact of the fire barrier would be less for the low emissivity conduit.

Impact of Emissivity on Base Line Ampacity Measurements

The emissivity of a conduit sample would directly impact the measurement of cable ampacity in an un-protected (base line) configuration. In general, radiative heat transfer is an important mechanism for the rejection of heat from a conduit. As the emissivity of a conduit increases, its ability to reject heat would also increase, and thus, the measured ampacity would increase.

In general, the most conservative approach to ampacity derating assessments is to maximize the base line current. In this way the relative impact of any changes to the system would also be maximized. Unfortunately, in the case of TUE, the surface properties of the tested conduits are not known. Hence, it is not known whether or not the base line test sample provided for a bounding assessment of base line currents.

In order to assess this question, SNL has performed simplified bounding calculations to assess the impact of changing surface emissivity on base line currents. The thermal model used for these calculations provides a one-dimensional calculatic · .: a sir. plified cable and conduit geometry in the absence of any fire barrier.

The thermal model assumes that only two resistance factors are active. First, there is a thermal contact resistance between the cable hot spot and the conduit. Hence, heat transfer from the cable to th. conduit is modeled as:

$$q_{cable-cond} = \frac{1}{R_{cable-cond}}(T_{cable} - T_{cond}) \tag{1}$$

The value of the cable-to-conduit resistance has previously been estimated by SNL based on TVA test results in which the ampacity, cable temperature, and conduit

temperatures were measured[1]. These estimated values will again be used in this analysis.

The conduit is assumed to be at a single uniform temperature throughout. This is a reasonable assumption given that the primary thermal resistance factors will be the cable-to-conduit and conduit-to-ambient factors. The resistance associated with conduction through the conduit would be quite small in comparison.

Heat transfer from the surface of the conduit is assumed to take place by a combination of convection and radiation. The convection (h_c) coefficient is estimated using a correlation for natural convection from a horizontal cylinder from Incropera and DeWitt (1990):

$$Nu_D = h_c \frac{D}{K_{air}} = \left(0.60 + \frac{0.387\ Ra_D^{1/6}}{\left[1 + (0.559/Pr)^{9/16}\right]} \right)^{8/27} \tag{2}$$

where (Nu_D) is the Nusselt number with diameter as the characteristic length scale, (D) is the diameter of the cable, (K_{air}) is the thermal conductivity of air, (Ra) is the Rayliegh number, and (Pr) is the Prandtl number. The rate of convective heat flux is then given by the expression:

$$q_c'' = h_c(T_c - T_a) \tag{3}$$

Finally, radiation from the outer surface is calculated as:

$$q_r'' = \epsilon\sigma(T_c^4 - T_a^4) \tag{4}$$

where (ϵ) is the emissivity of the conduit, and (σ) is the Stephan-Boltzmann constant. In practice the radiation equation is linearized by breaking down the forth power difference term as:

$$(T_1^4 - T_2^4) = [(T_1 + T_2)(T_1^2 + T_2^2)]\ (T_1 - T_2) \tag{5}$$

Using this relationship, a linearized radiative heat transfer coefficient (h_r) is calculated:

$$h_r = \epsilon\sigma[(T_c + T_a)(T_c^2 + T_a^2)] \tag{6}$$

and the radiative heat flux is then calculated as:

---

[1]See TVA test report 93-0501, 6/93 and SNL Draft letter report to the USNRC on JCN J2018, Task Order 2, March 15, 1994.

$$q_r'' = h_r(T_c - T_a) \qquad (7)$$

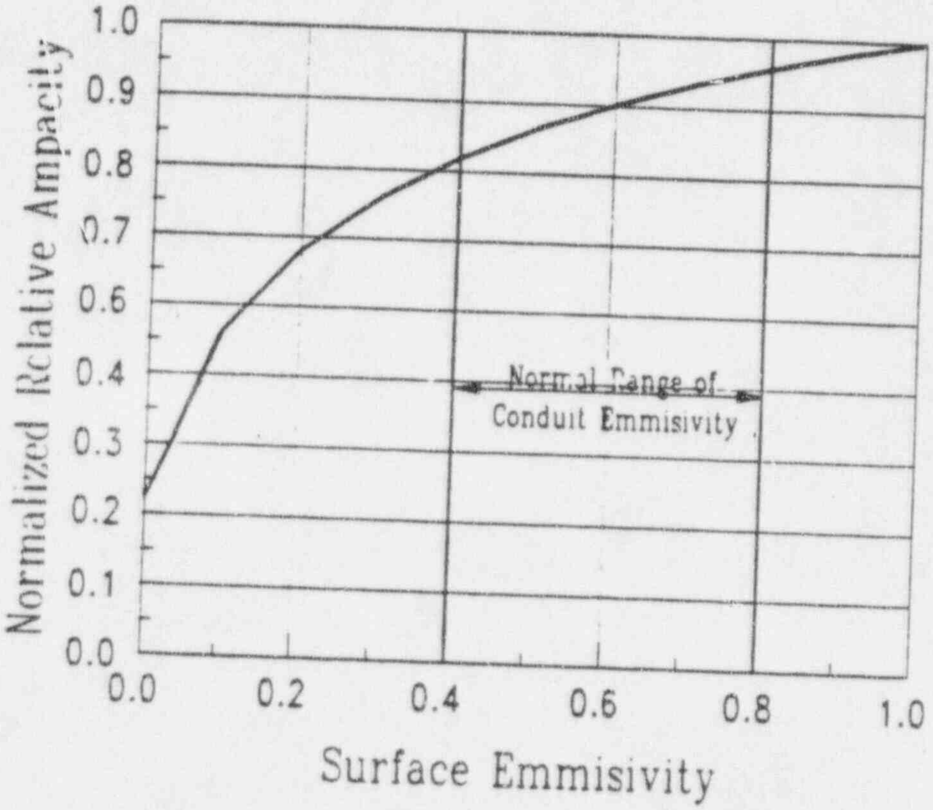

Figure 1: Impact of conduit surface emissivity on current carrying capacity of cables housed in a 1" conduit. Values have been normalized to value with (ε=1.0).

Note that in using this linearized relationship for radiative heat transfer, it is necessary to iterate until all of the calculated temperatures and heat fluxes match appropriately. The SNL calculations utilized a very simplistic iterative procedure to ensure that this match was achieved.

Using this analytical model, the impact of changing conduit surface emissivity on the ampacity of an unprotected conduit was assessed. The results are presented in Figure 1. These results assume a conduit diameter of 1", the same size as that used in the TVA tests on which the effective cable-to-conduit thermal resistance was estimated. Note that use of a small conduit for this calculation is also conservative because convective coefficients generally increase with conduit diameter for other factors

remaining constant. Hence, radiation is most important for the smaller conduits. The results have been normalized as an effective ampacity correction factor where the base line value is assumed to be the ampacity value with a conduit surface emissivity of unity (the maximum ampacity). This figure illustrate the importance of radiation to the overall heat rejection behavior of a conduit system.

Application of Analysis Results to TUE Tests

In reality, an emissivity value of 1.0 would never be achieved. In a meeting August 30, 1994, TUE stated that it had measured a range of conduits from its plant stocks and that the emissivity of these conduits varied between 0.4 and 0.8. These limits are considered typical of conduit behavior, and have been highlighted in Figure 1. Using these limits, the impact of realistic emissivity variations can also be estimated. Using the values from Figure 1 one can show that a cable housed in a conduit with an emissivity of 0.8, the upper end of the range, would be able to carry 119% of the ampacity limit for that same cable when housed in a conduit with an emissivity of 0.4, the lower end of the range.

This value, then, represents a reasonable upper bound on the extent to which TUE testing might have failed to bound its actual conduits. That is, if it is assumed that the TUE base line tests were performed using a low emissivity conduits (0.4) then the base line ampacities would have been minimized, reducing the ampacity derating factor. In order to bound the potential applications involving a high emissivity conduits (0.8) the base line test ampacities should be increased by approximately 19% and the derating factors recalculated accordingly. The same effect is achieved mathematically by simply taking the reported ampacity correction factors, and dividing by 1.19, the value cited immediately above.

Conclusions on Worst-Case Analysis

In testing, TUE's worst case conduit test found an ampacity correction factor of approximately 0.89 (or a derating factor of 11%). Using the bounding "correction" cited above, the lower bound ampacity correction factor would be (0.89/1.19=0.75) for a worst-case, bounding ampacity derating factor of approximately 25%. Table 1 summarizes similar "corrections" which have been imposed on all of the TUE conduit tests to encompass the emissivity uncertainty.

In considering these calculation, recall that several assumptions have been made. Most importantly, this analysis has assumed that, due to pure chance, the TUE's tests were performed using a combination of conduits which minimized the base line currents and maximized the clad currents. That is, this analysis has assumed that the base line conduit was low emissivity (0.4) while at the same time the clad conduit was high emissivity (0.8). This is reflected in the fact that the base line current was corrected while the clad current was not. This may well not have been the case, but in the

interest of bounding the potential impact, this is the worst-case scenario for the assessment of conduit emissivity effects on test results.

Table 1: Worst-case derating factors for TUE tests based on "corrected" base line ampacities to account for potential variations in conduit surface emissivity.		
Test Configuration:	Nominal Test ADF (%)	Worst-Case ADF (%)
3/C #10 AWG in 3/4" conduit	9.4	23.9
3/C #6 AWG in 2" conduit	6.6	21.5
4-1/C 750MCM in 5" conduit	10.7	25.0

Conduit Analysis Program Listing

The following pages provide a listing of the computer code utilized by SNL in its analysis of conduit thermal behavior. The code is not intended for general purpose utilization, and has received only minimal validation against experimental data. Its use here was primarily intended to provide for relative assessments on conduit surface emissivity effects rather than to provide predictions of actual conduit ampacity limits.

```
      PROGRAM CONDUIT2
C
C         written by:  Steve Nowlen, Sandia Nat'l Lab's
C                      February, 1995
C                      Revision:0
C
C This program simulates the thermal behavior of a conduit specimen
C loaded with powered cables.  No thermal barrier is simulated in this
C version.
C
C This version does calcs for given input set with emissivity varying
C between 0 and 1.0.  The values are also normalized to value at 1.0.
C
C User Input:
C      D = conduit outer diameter (in)
C      Thot = cable hot spot temperature (C)
C      Tamb = ambient temperature (C)
C      Rcc = cable-to-conduit thermal resistance (hr/BTU-F-ft))
C      Eps = conduit surface emissivity
C      Qguess = initial estimate of thermal power per foot of conduit
C              (BTU/hr-ft)
C
C based on these values, the program adjusts the Qguess value until a
C match between the internal and external heat transfer rates with the
C given temperatures is achieved.  The calculation proceeds from the
C inside to the outside
C
      REAL KAIR
      DIMENSION EPS(11),Q(11),QNORM(11),T(11)
      DATA EPS/1.,.9,.8,.7,.6,.5,.4,.3,.2,.1,0./
      PI=3.14159265359
C read input
      OPEN(UNIT=15,FILE='CONDUIT2.IN',STATUS='OLD')
      READ(15,*)D,THOT,TAMB,RCC,QGUESS
      CLOSE(15)
C do units conversions to make consistent:
C change temps from C to R:
      THOTR=CTOR(THOT)
      TAMBR=CTOR(TAMB)
      WRITE(6,903)THOTR,TAMBR
 903  FORMAT(' THOTR,TAMBR ',2E14.5)
C convert D inches to feet:
      D=D/12.
C calculate perimeter:
      PERIM=PI*D
C set up do-loop:
      DO 20 I=1,11
C start iterations:
      ITER=1
 10   CONTINUE
C calculate conduit temp given Q and Rcc:
      TCOND=THOTR-QGUESS*RCC
C given Tcond, calculate Q to ambient:
C external convection:
      TAVG=(TCOND+TAMBR)/2.
      CALL PROPS(AVG,GBNA,PR,KAIR)
      RAY=GBNA*ABS(TCOND-TAMBR)*D**3
      PART1=0.387*RAY**(1/6)
      PART2=(1+0.559/PR)**(9/16)
      HCONV=KAIR/D*(0.60+PART1/PART2)**(8/27)
```

```
C external radiation:
      HRAD=EPS(I)*0.172E-8*(TCOND**2+TAMBR**2)*(TCOND+TAMBR)
C total heat transfer coeff:
      HTOTAL=HCONV+HRAD
      REXT=1./(HTOTAL*PERIM)
C total external
      QEXT=(TCOND-TAMBR)/REXT
C compare to internal and correct
      TOLER=ABS((QGUESS/QEXT)-1.)
C check for convergence
C print and iterate or done:
      TCONDC=RTOC(TCOND)
      IF(TOLER.GT.0.0001) THEN
        WRITE(6,901)QGUESS,QEXT,TCONDC,ITER
901     FORMAT(' Qguess,Qext,Tcond,ITER ',3E14.5,I5)
C correct Qguess as per average of two values:
        QGUESS=(QGUESS+QEXT)/2.
        ITER=ITER+1
      GOTO 10
        ELSE
        WRITE(6,902)QGUESS,QEXT,THOT,TAMB,TCONDC,EPS(I)
902     FORMAT(' SOLUTION FOUND !!!',/,
     &  ' Qguess,Qext,Thot,Tamb,Tcond,Eps '
     &  ,/,' ',6E14.5)
        ENDIF
C end of do-loop 20, save values:
      PAUSE
      Q(I)=QGUESS
      QNORM(I)=Q(I)/Q(1)
      T(I)=TCONDC
20    CONTINUE
      WRITE(6,904)
904   FORMAT(' FINAL SOLUTION VECTOR:',/,
     & '   Q           Qnorm         Tcond        Eps')
      DO 30 I=1,11
        WRITE(6,905)Q(I),QNORM(I),T(I),EPS(I)
905     FORMAT(4E14.5)
30    CONTINUE
C
      STOP
      END
C
      SUBROUTINE PROPS(TEMP,GBNA,PR,XK)
C This subroutine simply provide for the extrapolation of air
C property values between four tabulated values.  A linear
C extrapolation between table values is used.
      DIMENSION RHO(4),PRT(4),TTAB(4),XMU(4),XNU(4),COND(4),BETA(4)
     &  ,AL(4)
C thermal diffusivity
      DATA AL/.646,.720,.905,1.20/
C temperature indexes (F)
      DATA TTAB/0.,32.,100.,200./
C Prandtl number
      DATA PRT/0.73,0.72,0.72,0.72/
C density
      DATA RHO/.086,.081,.071,.060/
C viscosity
      DATA XMU/1.11E-5,1.165E-5,1.285E-5,1.440E-5/
C kinematic viscosity
      DATA XNU/.130E-3,.145E-3,.180E-3,.239E-3/
```

```
C thermal conductivity
      DATA COND/0.0133,0.0140,0.0154,0.0174/
C coef of thermal expansion
      DATA BETA/2.18E-3,2.03E-3,1.79E-3,1.52E-3/
C  convert to F as per tables from Kreith
      T=TEMP-460.
C do extrapolations:
      DO 10 I=2,4
        IF(TTAB(I).GT.T)GO TO 20
10    CONTINUE
      I=4
20    FRACT=(T-TTAB(I-1))/(TTAB(I)-TTAB(I-1))
      PR=PRT(I-1)+FRACT*(PRT(I)-PRT(I-1))
      B=BETA(I-1)+FRACT*(BETA(I)-BETA(I-1))
      XN=XNU(I-1)+FRACT*(XNU(I)-XNU(I-1))
C      XM=XMU(I-1)+FRACT*(XMU(I)-XMU(I-1))
C      R=RHO(I-1)+FRACT*(RHO(I)-RHO(I-1))
      XK=COND(I-1)+FRACT*(COND(I)-COND(I-1))
      A=(AL(I-1)+FRACT*(AL(I)-AL(I-1)))/3600.
C GBNA is g*beta/(alpha*nu), it is a convenient value used
C to simplify calculation of Rayliegh number for conv. relations
      GBNA=32.2*B/(A*XN)
      RETURN
      END
C
      REAL FUNCTION CTOR(TC)
C converts temperature from C to R
      TF=(TC+40.)*9./5.-40
      TR=TF+460.
      CTOR=TR
      RETURN
      END
C
      REAL FUNCTION RTOC(TR)
C converts temperature from R to C
      TF=TR-460.
      TC=(TF+40.)*5/9-40.
      RTOC=TC
      RETURN
      END
C
C ***********  END OF PROGRAM **********
```