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Experimental Study of Heat Transfer to NaK Flowing In-Line
Through Tightly Packed Rod Bundles

AUTHOR

A. R. Marchese

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ABSTRACT

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*	R. O. Williams	LB13
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An experimental heat transfer study is reported for the case of longitudinal flow of NaK through tightly packed rod bundles having pitch-to-diameter (P/D) ratios of 1.00 and 1.05. Although the flow was turbulent, the Peclet number range was low enough such that eddy conduction was negligible. Each rod bundle consisted of 19 cylindrical rods arranged on hexagonal arrays. All rods in the bundle were electrically heated to provide equal and axially uniform heat flux. To determine the rod circumferential temperature distribution, small diameter thermocouples were welded to the inside cladding tube on all heaters. In addition, sufficient instrumentation was provided to measure the coolant temperature distribution at the inlet and outlet to the rod bundle, the power supplied to each rod, and the coolant flow rate.

Fully established heat transfer results for the cladding azimuthal temperature distributions and rod-average film coefficients were obtained over a range of power and flow conditions of interest to the ZrH reactor. For the P/D = 1.00 test configuration, the experimental results were found to be in excellent agreement with theoretical predictions. In the flow range investigated, the rod azimuthal temperature gradients and Nusselt numbers were shown to be independent of the Peclet number, unlike the previous test results of Subbotin et al. in the USSR. In comparing theoretical and experimental results for the P/D = 1.05 rod arrangement, good agreement was obtained for the rod average film coefficients; however, poor agreement was found for the cladding circumferential temperature gradients. The test results for P/D = 1.05 showed essentially no azimuthal temperature gradients. This was attributed to a significant increase in the fluid turbulence intensity caused by the presence of spacer rings, which were used to maintain a symmetrically spaced rod bundle.

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A. INTRODUCTION

This report presents the results of an experimental study of heat transfer to NaK flowing in-line through rod bundles of small pitch-to-diameter (P/D) ratios. Figure A.1 depicts the geometry of interest. These studies are part of Atomics International's Zirconium Hydride (ZrH) Reactor Program and were intended to provide information needed in the design and evaluation of various liquid metal-cooled, compact nuclear reactor concepts. Prior to this investigation, recent analytical studies ^{1,2,3} have shown that large circumferential temperature and heat flux gradients can exist for heat transfer to liquid metals in turbulent longitudinal flow through closely spaced ($P/D \leq 1.10$) rod bundle arrays. In addition, appreciable coolant velocity variations in the azimuthal direction have been shown ^{4,5} to exist. These peripheral gradients result in a much lower convective heat transfer coefficient than exists for rod bundles of high P/D ratios. The large azimuthal temperature, heat flux, and velocity variations are the result of the irregular tricuspid coolant flow passages, as shown in Fig. A.2, which exist for closely packed rods with an equilateral triangular arrangement. As the rod spacing or P/D ratio is increased, the coolant flow passages become more regular; the peripheral heat transfer and fluid dynamic gradients decrease; and the heat transfer coefficient increases.

For turbulent longitudinal flow through loosely spaced rod bundles, liquid metal heat transfer results of Nusselt number (Nu) vs Peclet number (Pe) exhibit on a log-log plot the characteristic "dog-leg" shape, as is shown in Fig. A.3. The results of Fig. A.3 have been predicted theoretically by Dwyer and Tu⁶ and verified experimentally by Maresca and

Figure A.1 Cross section of a seven-rod portion of an infinite fuel rod bundle with equilateral triangular arrangement

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Figure A.2 Typical section of a closely packed rod bundle depicting irregularity of coolant flow area. Cross-hatched area represents 1/12 repeating 30° section

Figure A.3 Rod bundle heat transfer results of Dwyer et al.^{6,7}

Dwyer⁷. The characteristic profile is due to a molecular conduction dominated regime, which is essentially independent of Pe , and a molecular-plus-eddy conduction regime, which is flow or Pe dependent.

For closely spaced rod arrays where the $P/D < 1.10$, the same characteristic "dog-leg" profile has been predicted to exist; however, experimental verification of this is nonexistent due to a scarcity of published results. Theoretical studies^{1,2,3,8,9,10,11} have been performed for closely spaced ($P/D < 1.10$) rod bundles assuming negligible eddy conductivity in the molecular conduction dominated regime. These analyses differ as to the number of fuel element regions that are considered in the energy balance and assumptions regarding laminar, slug, or turbulent flow of the coolant. For the high Pe regime where eddy conduction becomes important, recent theoretical studies of Oberjohn¹² and Dwyer¹³ have predicted Nu vs Pe results for P/D ratios < 1.10 which exhibit the characteristic "dog-leg" profile. However, until sufficient experimental data becomes available for closely spaced rod bundles, care must be exercised in using the results of Refs. 12 and 13 due to the assumptions regarding the two-dimensional eddy thermal diffusivities ($\epsilon_{H_r}, \epsilon_{H_e}$) and the single-region method of solution.

Subbotin et al. in the USSR are the only researchers who have published experimental results for liquid metals flowing through rod bundles with the $P/D < 1.10$. For the case of $P/D = 1.00$, i.e., where the rods are contacting each other, Subbotin et al.^{14,15} present heat transfer results for both Hg and NaK. Their Nusselt number results, shown in Fig. A.4, exhibit a Peclet number dependence which should not exist in the flow range investigated. Since the authors did not mention the oxygen content of the liquid metals and the steps taken to insure that the experimental loop was very clean, Dwyer¹⁶ suggests the possibility of contaminants and poor thermal contact at the wall. This

Fig. A.4 EXPERIMENTAL DATA OF
SUBBOTIN ET AL 1955

$$P/D = 1.00$$

$$(R-r_3)/R = 0.227$$

—○— Hg DATA ($k_w/k_f = 1.80$) ; Ref. 15

Nu

4.00

2.00

1.00

0.80

0.60

0.40

0.20

0.10

--- EMPIRICAL EQUATION:

$$Nu = 0.15 (k_w/k_f)^{0.02} Pe^{0.3} + 0.04 \sqrt{k_w/k_f}$$

EVALUATED FOR NaK ($k_w/k_f = 0.872$) ;
Ref. 14

10'

2

3

5

7

Pe

10²

2

3

5

7

10

would cause the Nu results to be lower than would exist for a perfectly clean system and explain the Pe dependence shown in Fig. A.4. In addition, Subbotin's results were obtained with the use of relatively thick cladding wall (0.078 in.) test rods. This results in considerably greater film coefficients than would exist for thin wall heater elements, which as will be explained later, provide a better thermal simulation of a ZrH fuel element.

For the present study, a rod bundle consisting of 19 electrically heated elements was tested under conditions of turbulent longitudinal flow of NaK. The rods were arranged on symmetrical equilateral triangular arrays. Two rod bundle configurations, corresponding to P/D ratios of 1.00 and 1.05, were tested. Although the flow was turbulent, the Peclet number range was low enough such that molecular conduction was the dominant mode of heat transfer. To determine the rod azimuthal temperature distribution, small diameter thermocouples were welded to the inner cladding wall on all heaters. Sufficient instrumentation was also provided to measure the test section inlet and outlet NaK temperatures, the power supplied to each rod heater, and the coolant flow rate. From the above measurements, rod-average heat transfer coefficients and Nusselt number variations with Peclet number were determined.

B. THEORETICAL CONSIDERATIONS

An analytical model which is the theoretical complement to the present experimental study is described in detail in Ref. 1. The analytical model was used in the design of the experimental test rods and in predicting the heat transfer results of the present investigation. Since only a brief description will be presented here, it is suggested that the reader become familiar with the theoretical study of Ref. 1.

For close rod spacings as depicted in Fig. A.2, the irregular shape of the coolant flow area creates higher temperatures in the lower (narrow) portion of the channel than in the upper (wider) portion. For P/D near 1.000, the azimuthal temperature variation in the coolant stream becomes large and is reflected back into the interior of the fuel element. The extent of the reflection depends on the thermal conductivities and thicknesses of the various fuel element regions. To obtain an analytical solution for P/D near

1.000, the differential energy equations describing the heat transfer within the coolant stream and various fuel element regions must be solved simultaneously in conjunction with appropriate boundary conditions. This is the

so called "multi-region" method of solution, and no assumptions are made regarding the constancy of temperature or heat flux at any of the boundary surfaces.

For an equilateral triangular rod arrangement, it is possible to isolate a 30 degree repeating section of a fuel element and associated coolant channel. This is depicted by the cross-hatched area shown in Fig. A.2. An isolated symmetrical section of the fuel element and associated coolant channel, which was chosen for study, is shown in Fig. B.1. The fuel element model, Fig. B.1, has provisions for considering up to five different material regions. For the

Figure B.1 Symmetrical section of a five-region fuel element

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study presented in Ref. 1, three, four and five-region fuel elements were considered.

The governing energy equations for the various fuel element regions are

$$k_F \nabla^2 T_F + q_F''' = 0 \quad (1)$$

$$\nabla^2 T_1 = 0 ; \nabla^2 T_2 = 0 ; \nabla^2 T_3 = 0 \quad (2,3,4)$$

$$k_f \nabla^2 T_f = \rho C_p u \partial T_f / \partial z \quad (5)$$

where the Laplacian operator, ∇^2 , in cylindrical coordinates is given by

$$\nabla^2 = \frac{1}{r} \frac{\partial}{\partial r} \left(r \frac{\partial}{\partial r} \right) + \frac{1}{r^2} \frac{\partial^2}{\partial \theta^2}$$

Equations (1) through (5) are based on the assumptions of no axial conduction, temperature invariant properties, fully-developed temperature and velocity profiles, and turbulent flow with negligible eddy-thermal diffusivities ($\epsilon_{H_r}, \epsilon_{H_\theta}$). The appropriate boundary conditions which represent the physical requirement of temperature and heat flux continuity at the interfaces of the different regions are

$$T_F = T_1 ; k_F \frac{\partial T_F}{\partial r} = k_1 \frac{\partial T_1}{\partial r} , \text{ at } r=r_1 \quad (6a, b)$$

$$T_1 = T_2 ; k_1 \frac{\partial T_1}{\partial r} = k_2 \frac{\partial T_2}{\partial r} , \text{ at } r=r_2 \quad (6c, d)$$

$$T_2 = T_3 ; k_2 \frac{\partial T_2}{\partial r} = k_3 \frac{\partial T_3}{\partial r} , \text{ at } r=r_3 \quad (6e, f)$$

$$T_3 = T_f ; k_3 \frac{\partial T_3}{\partial r} = k_f \frac{\partial T_f}{\partial r} , \text{ at } r=R \quad (6g, h)$$

Also, referring to Fig. B.1, the following symmetry conditions apply:

$$\frac{\partial T_f}{\partial \theta} = 0, \text{ at } \theta = 0 \text{ and } \pi/6 \quad (6i)$$

$$\frac{\partial T_f}{\partial r} = \frac{\tan \theta}{r} \frac{\partial T_f}{\partial \theta}, \text{ at } r = \frac{P}{2 \cos \theta} \quad (6j)$$

It is evident that Eqs. (1) through (5) are coupled by the boundary conditions of Eqs. (6a) through (6h).

The fully-developed velocity profiles, $u = f(r, \theta)$, must be determined in order to solve the coolant energy balance, Eq. (5). The empirical velocity relations of Levchenko et al.⁴ and Eifler and Nijsing⁵ were used. For flow through rod bundles having P/D ratios of 1.00 to 1.15, these authors found that they could correlate measured radial velocity profiles at each circumferential angle (θ) with Von Karman's law-of-the-wall equations.

$$u^+ = y^+, \quad (y^+ \leq 5) \quad (7a)$$

$$u^+ = -3.05 + 5.0 \ln y^+, \quad (5 < y^+ \leq 30) \quad (7b)$$

$$u^+ = 5.5 + 2.5 \ln y^+, \quad (y^+ > 30) \quad (7c)$$

provided the shear stress (τ_w) in u^+ was a local value, $\tau_w(\theta)$, measured at a given peripheral location (θ). Equations (7a) through (7c) were used to calculate the velocity profiles which were used in Eq. (5). For $Re \approx 22,000$, Fig. B.2 shows the velocity profile in dimensionless form that was determined for a rod bundle having P/D = 1.00. Although the results of Fig. B.2 were determined for a specific Reynolds number, the dimensionless velocity profiles, which were measured in Refs. 4 and 5, were found to be relatively insensitive to Re over the range ($14,000 \leq Re \leq 95,000$) investigated.

Simultaneous solution to Eqs. (1) through (5) was obtained numerically with the aid of a general thermal analyzer digital computer code. A finite difference method is used in the computer code to calculate either transient or

Figure B.2 Dimensionless turbulent velocity distribution for a rod bundle having a $P/D = 1.00$ with $Re \approx 22,000$

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steady state temperatures. The code was used in conjunction with a detailed nodal model of a 30 degree symmetrical section of a fuel element and associated coolant channel. Figure B.3 shows a schematic of a nodal grid for a five-region fuel element. Solutions for three and four-region fuel elements were obtained with the same grid by changing the thermal conductivities and material thicknesses accordingly. In order to satisfy Eqs. (6a) through (6h), iteration with the computer code was performed until the temperatures and heat fluxes at the interfaces of the various fuel element regions matched.

By obtaining solutions over a range of thermal conductivities and thicknesses of the various fuel element regions, the effects of these parameters on heat transfer to liquid metals flowing through tightly packed rod arrays are shown in Ref. 1. Dimensionless results are presented for the circumferential temperature and heat flux distributions at the interfaces of the various fuel element regions. From these results, rod-average film coefficients and Nusselt numbers are presented in Ref. 1 for a range of fuel element parameters of practical interest.

Besides the results presented in Ref. 1, additional calculations were made to predict the results corresponding to conditions of the present study. Three-region calculations were made for the case of electrically heated test rods arranged on hexagonal arrays having P/D ratios of 1.00 and 1.05. The five-region nodal model, shown in Fig. B.3, was modified accordingly for a three-region test rod. The test rod regions consisted of MgO electrical insulation, stainless steel cladding, and NaK coolant. The heater element design will be discussed in detail in the following sections. The coolant region, which is shown in Fig. B.3 for P/D = 1.00, was changed accordingly for the P/D = 1.05 calculations to include the larger coolant area. For

Figure B.3 Five-region fuel element nodal model;
greatly enlarged version of coolant
nodal network is shown isolated

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various power levels examined in the experimental investigation, Fig. B.4 shows the predicted results for the circumferential temperature distribution on the inner cladding wall of a test rod arranged in a rod bundle having a $P/D = 1.00$. The corresponding predictions for a rod bundle have a $P/D = 1.05$ are shown in Fig. B.5. For the two test configurations, the predicted results for the rod-average film coefficients are shown in Fig. B.6 in dimensionless form of Nusselt number vs Peclet number.

Fig. B.5 PREDICTED AZIMUTHAL VARIATION OF
INSIDE CLADDING WALL TEMPERATURE

C. FUEL ELEMENT AND CORE THERMAL SIMULATION

For the present heat transfer investigation, primary emphasis was placed on providing a good thermal simulation of a ZrH core by using a bundle of electrically heated test rods. For rod bundle heat transfer studies at high P/D ratios, simulation of a reactor core can be provided by simply duplicating flow and heating conditions for a particular rod arrangement. The internal physical characteristics of the test rods are usually not important at high P/D ratios; however, this is not the case for P/D ratios near 1.00, where heat transfer coupling exists between the coolant stream and internal heater regions. For P/D near 1.00, thermal simulation becomes more complex because of the dependence of the azimuthal heat transfer gradients on the thermal and dimensional characteristics of the heater element.

Before proceeding, a brief description of a ZrH fuel element and core is warranted. A typical ZrH core consists of a bundle of closely spaced ($1.00 \leq P/D \leq 1.05$) fuel rods arranged on hexagonal arrays. The core is cooled by a liquid metal, NaK-78, and the Peclet number, which characterizes the coolant flow, extends over the range of 30 to 150. Heat fluxes up to 10^5 Btu/Hr-Ft² and coolant temperatures in the range 1000°F - 1200°F are representative. The fuel rods, which are massively hydrided, consist of an alloy of zirconium and uranium. The fuel rods are clad in thin wall tubing; Hastelloy-X and Inconel-800 tubing with wall thicknesses in the range of 0.010 in. - 0.030 in. are being considered. The clad tubing is coated internally with a 2-mil thick ceramic barrier to inhibit hydrogen moderator leakage. To accommodate fuel rod swelling and thermal expansion, a hydrogen gas gap, with a thickness in the range of 3 to 6-mils, exists between the fuel rod and cladding tube. Typical values for the outer diameter and active length of the fuel element are 9/16 in. and 17 in., respectively.

For P/D near 1.00, all regions of the fuel element must be accounted for in the thermal analysis. Therefore, to describe the heat transfer characteristics of a ZrH fuel element, the five-region analytical model, shown in Fig. B.1, was chosen for study in Ref. 1. The gas gap, ceramic barrier, and cladding tube are represented by regions 1, 2 and 3, respectively. Including the fuel and coolant sections gives a total of five-regions. To determine the effect on heat transfer, results were determined in Ref. 1 for a range of thermal conductivities and thicknesses of the various ZrH fuel element regions. The results of Ref. 1 showed that the magnitude of the circumferential temperature and heat flux gradients, which determine the rod-average film coefficient, are dependent upon the ratios of conductivities and thicknesses of the various fuel element regions. By increasing the thermal conductivity and thickness of a fuel element region, circumferential heat flow was enhanced. Improving the circumferential heat flow through a region resulted in a decreased azimuthal temperature gradient and an increased heat transfer coefficient.

Without the presence of a gas gap, results were presented in Ref. 1 for a four-region ZrH fuel element. Comparison of the four and five-region results showed that the influence of a gas gap on heat transfer is appreciable. The gas gap, in effect, insulates the fuel from the cladding resulting in reduced azimuthal heat flow through the fuel region. With the fuel in contact with the cladding, there is greater circumferential heat flow, smaller cladding peripheral temperature variation, and a greater average heat transfer coefficient. From the standpoint of thermal simulation, the existence of a H_2 gas gap in a ZrH fuel element had an important influence on the selection of a suitable test rod.

To closely simulate the thermal behavior of a ZrH fuel element with an electrically heated test rod, the azimuthal variations of temperature and heat flux must be closely matched under identical coolant flow, heating, and geometrical conditions. To reproduce the peripheral heat transfer gradients, the thermal conductivities and thicknesses of the different regions of the heater element must bear a certain relationship to one another. A typical heater element consists of an electric coil surrounded by a suitable electrical insulator and clad in a metal tube. The three-region fuel element results of Ref. 1 can be used to describe the heat transfer characteristics of a heater element in a tightly packed array.

From the parametric results presented in Ref. 1 for a three-region fuel element, a heater element design can be determined which closely simulates a five-region ZrH fuel element. This is accomplished by comparing the azimuthal temperature and heat flux gradients until a close match is made between the three and five-region results. An example is presented in Ref. 1 for a typical ZrH fuel element and a heater element. It was determined that a test rod with a low thermal conductivity region adjacent to the cladding, i.e., a small k_f/k_w ratio, would be desirable to closely simulate the effects of the ZrH fuel element gas gap. Since the thermal conductivity of porous magnesium oxide (MgO) is relatively low, it was used as the electrical insulator for the heater rods. The ratio of cladding-to-coolant thermal conductivities (k_w/k_f) and relative cladding thickness $[(R-r_3)/R]$ are other important parameters involved in fuel element simulation. It was determined that the combination of 304 SS cladding and NaK-78 coolant for a heater rod would closely match the conductivity ratio of either Hastelloy-X or Inconel-800 and NaK-78 for a ZrH element. To simulate the thin cladding wall thickness

range (0.010 in. - 0.030 in.) of interest to a ZrH fuel element, a 0.020 in. wall was chosen for the 304 SS tubing used for the heater cladding. By careful selection of the parameters - k_F/k_W , k_W/k_F , $(R - r_3)/P$ - as explained above, a heater element design was determined which simulates the thermal behavior of a ZrH fuel element within 10% under identical flow, heating and geometrical conditions. A more complete design description of the test rods will be given in the next section where the experimental equipment is described.

D. EXPERIMENTAL EQUIPMENT

D.1 NaK Loop

Testing was conducted in a loop facility specifically designed for liquid NaK heat transfer studies. A detailed description of the loop facility is provided in Ref. 17; only a brief description will be given here. A schematic flowsheet and an isometric drawing of the loop are shown in Figs. D.1 and D.2. A portion of the actual loop facility is shown in Fig. D.3. The test loop was fabricated with all stainless steel (SS) piping and components and has an all-welded construction. The main loop flow line is made of $1\frac{1}{2}$ in., schedule 10, 316 SS pipe, while the branch lines, such as the fill and drain lines, are made of $1/2$ in., schedule 10, 316 SS pipe. Before fabrication of the loop, the piping and components were carefully cleaned with an acetone solution. All welding was done under an argon atmosphere. After fabrication and before filling with NaK, the loop was kept under a positive pressure of pure argon. The entire loop is insulated with at least three inches of high temperature asbestos insulation.

The loop is capable of operating at temperatures up to 1300°F and flow rates up to 30 gpm against a $4\frac{1}{2}$ psi head.

Pumping power is supplied by three parallel D.C. conduction pumps in series with an electromagnetic linear induction pump. Heat rejection from the loop is accomplished by a 135 Kw capacity air blast heat exchanger. A 150 Kw in-line NaK heater assembly is located in the main stream of the loop. Flow rates are determined by means of a calibrated permanent magnet flowmeter.

Two expansion tanks which are also used as surge tanks are located at the highest point in the loop. The tanks are instrumented to measure the

Fig.D.1-Zr H HEAT TRANSFER NaK LOOP-MODIFIED

Fig. D.2. ZrH HEAT TRANSFER STUDY TEST LOOP

Figure D-3. Experimental Loop Facility for NaK
Heat Transfer Studies

7759-40768

NaK liquid levels and the corresponding pressure drop across the test bundle. Located at the lowest point in the loop is a drain tank which is provided with a 1/2 in. drain line, a 1/2 in. fill line with a 5 micron filter arrangement and a liquid level probe. System pressure is maintained with argon cover gas plumbed to both the expansion and drain tanks with associated pressure gages, shut off and vent valves, and a pressure relief valve. The piping is so arranged that pressures in the drain and expansion tanks can be independently controlled.

A flow-through cold trap is located off a 1/2 in. diameter branch line paralleling the main stream line. Water which serves as the cooling medium flows through 3/8 in. copper tubing which is wrapped around the cold trap. The flow through method eventually passes all the NaK through the refrigerated region causing impurities to adhere to the cold surface.

In order to perform a chemical analysis for impurities in the circulating NaK, a sampling section (1/2 in. O.D. tube) is located in a branch line off the main flow line. Isolation valves and swagelock fittings are provided at both ends of the NaK sampling tube.

Loop temperatures are monitored by chromel-alumel thermocouples which are located at strategic points throughout the loop facility. In the event of a malfunction or an abnormal operating condition, the loop facility is provided with an automatic shutdown and alarm system.

D.2 Test Section

The test section consisted of a bundle of 19 electrically heated cylindrical elements, or rods, arranged on symmetrical equilateral triangular arrays. Two rod bundle configurations corresponding to P/D ratios of 1.00 and 1.05 were investigated. The same 19 heaters were used for each configuration. Each rod bundle was contained within a 304 SS cylindrical shell which had half round

filler rods attached to the inner wall. Figure D.4 shows a design layout of the test section assembly for all rods in contact, $P/D = 1.00$. The actual test section components are shown in Figs. D.5, D.6, and D.7. The $P/D = 1.05$ test section assembly is similar with the addition of 0.030 in. thick rings on each rod at three different axial locations, as shown in Fig. D.8, to maintain proper spacing of the rods. The half round filler rods, which are shown in Fig. D.9, were attached to the I.D. of the shell by screws and could be adjusted radially to maintain a tight and symmetric rod arrangement.

To achieve a uniform velocity profile at the entrance to the rod bundle, a plenum and bellmouth nozzle arrangement, shown in Fig. D.4, were used at the inlet to the test section. Because of the rapid coolant acceleration in flowing from the plenum into the bellmouth nozzle, the fluid boundary layer remains attached to the wall and relatively thin causing the pressure loss to be small and the nozzle exit velocity profile to be uniform. Upstream of the test section inlet, the $1\frac{1}{2}$ in. main loop line is connected to a 3 in., schedule 40, 90° long tangent elbow which in turn is connected to the inlet plenum. The sudden expansion into the plenum serves to break up the distorted velocity profile region that is created in the elbow region. Also, to prevent any jet action from impinging on the test section entrance, a baffle arrangement, shown in Fig. D.4, is inserted inside the plenum just aft of the elbow exit.

To determine an accurate film coefficient, it is desirable to maintain a uniform bulk coolant temperature distribution across the test section. The peripheral coolant channels, which are adjacent to the half round filler rods, receive heat input from either one or two rods, while the interior channels receive heat from three rods. To keep the bulk coolant temperature

1. MATERIALS:
 A. ASME COMPRESSOR COMPONENTS
 TYPE 304 S.S.TL
 B. WOODWARD FITTINGS TYPE
 304 GR 316 S.S.TL
 C. BOLTS TYPE 304 OR 316 S.S.TL
 D. HEATER CLAD TYPE 304 S.S.TL
 HEATER TIC CLAD TYPE 304 S.S.TL
 HEATER TC WIRE, CHROMEL/NI
 E. WELDING FLUID, ALK

2. TEMPERATURES
 A. INLET 1100°F
 B. OUTLET 1500°F

3. MAX FLOW RATE, 306 MM

4. HEATER DATA:
 A. VOLTAGE 100
 B. OPERATING POWER 5.0 KW
 C. CLAD THICKNESS .020" ± .003
 D. LEAK TIGHT TO THE
 REQUIREMENTS OF
 SPEC 30115 NAD021

5. REFERENCE DOCUMENTS:
 A. LETTER TO AT-535
 B.

6. WELDING:
A WELDING TO BE PERFORMED
BY PERSONNEL HAVING A
CURRENT ASME BOILER AND
PRESSURE VESSEL CODE
SECT. IX CERTIFICATION FOR
THE MATERIAL, FILLER
PROCESS AND POSITION.
B. PENETRANT INSPECT ALL
WELDS TO THE REQUIREMENTS
OF NAVJIS-001, TYPE II,
CLASS 3. NO CRACKS
PERMITTED NO POROSITY VOIDS
GREATER THAN 1/32 IN. DIA.
C. HELIUM LEAK TEST FINAL
ASSEMBLY TO THE REQUIRE-
MENTS OF SPEC. STANISNADOT.
7. PACKAGING:
A. HEATER, PACKAGE PER
SPEC. NAVJIS-001, PARA 12,
B.1.5, B.1.6, CLEANING PER
PARA A.1.1.12 THIN PROTECTON
AND PRESERVATION PER PARA
A.1.2.3, A.1.5, B.1.5.3, AND 5.
B. SEE SHEET 2 FOR SOUND,
SYMMETRICAL AND
ASYMMETRICAL CONFIGU-
RATIONS.

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[illegible]

Figure D-5. Assembly of 19 Heater Elements

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Figure D-6. Cylindrical Shell Containing 19 Test Rods

Figure D-7. Other Test Section Components

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1 THE INTERPRETATION PER STONEMAN
2 CLEAN -DE BY KENNETH IN ALL TIME
3 IDENTITY-RE PER ANDERSON (TWO)

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Figure D.8 Spacer ring detail
for $P/D = 1.05$
test configuration

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Figure D-9. Half Round Filler Rods

7759-40766

rise for the outer edge channels the same as the interior channels, the flow rate through the peripheral channels had to be reduced in direct proportion to the reduced heat input. This was accomplished by inserting small diameter wires into the outer edge channels, as shown in Figs. D.4 and D.9. The wires were sized such that the resulting coolant flow distribution would result in equal coolant temperature rise across the rod bundle.

To keep heat losses to a minimum, the outer wall of the test section was insulated with two inches of fiberfrax under three inches of high temperature asbestos insulation.

All 19 rods were special electric heaters which were designed to simulate ZrH fuel elements. A design layout of a heater element is shown in Fig. D.10. The test rods consist basically of a 1/4 in. O.D. nichrome coil encased in magnesium oxide electrical insulation which is swaged into a 0.020 in. wall, 9/16 in. O.D., 304 SS tube. Up to 5 Kw of power can be supplied to each heater; this corresponds to an outer surface heat flux of 139,000 Btu/Hr-Ft² which is maintained uniformly over the 10 in. heated length. The electrical resistances of all heaters agreed to within 1/2%, and the rods were radiographed to check both the concentricity and uniformity of the nichrome coil. As is evident from Figs. D.4 and D.10, the aft end of the test rods were swaged down to 1/2 in. O.D. and were bent to varying degrees depending on their position in the bundle. This was done to allow more room at the top head for the rods to penetrate the test section through standard 1/2 in. swage lock fittings.

A complete summary of test section statistics are listed in Table I.

As previously described, a heater element employing MgO electrical insulation and a 20-mil wall, SS tube which is cooled by NaK was selected to

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TABLE I

Test Section Statistics

1. Number of rods in bundle	19	
2. Outer diameter of rods, in	9/16	
3. Geometrical arrangement	Equilateral Triangular	
4. Length of rods (9/16 in. O.D. region), in	15.5	
5. Heated length, in	10.0	
6. Pitch-to-diameter ratio	1.00	1.05
7. Hydraulic diameter, in	0.0574	0.1256
8. z_F/D_h (Cladding T/C location with respect to velocity profile development)	183.	84.
9. z_H/D_h (Cladding T/C location with respect to temperature profile development)	139.	64.
10. z_S/D_h (Cladding T/C location with respect to middle spacer rings)		32.

provide a good thermal simulation of a ZrH fuel element. Although MgO is a standard electrical insulator used for these types of heaters, the thin (20-mil wall) cladding was, to the present author's knowledge, the first time that a tube thickness < 0.050 in. was used in a rod bundle heat transfer investigation. During early fabrication of the heater rods, there was a problem associated with the thin wall tubing. Cracks in the tubing occurred during the swaging and bending operations. Later refinements in the swaging and bending operations overcame this problem.

D.3 Instrumentation

Small diameter thermocouples were used to monitor the temperature on the inside surface of the heater cladding. The thermocouples are accurately located and welded to the cladding I.D. at AI prior to filling and fabricating the heater at Watlow Electric Mfg. Co. Up to 9 thermocouples are spaced around the inside cladding periphery to measure the rod circumferential temperature distribution.

The thermocouples which are used for the cladding temperature measurements consist of chromel and alumel wires, surrounded by MgO insulation, and sheathed in Inconel 600 tubing with an O.D. of 0.020 in. They are fabricated with a special measuring junction which is electrically grounded and flattened to 0.010 in. thick. This thin tip is welded to the inside of a 0.020 in. wall x 0.625 in O.D. tubing which subsequently becomes the heater sheath.

After the thermocouples are fabricated, they are located at the end of a 10.5 in. long tube at specific azimuthal locations and held in place before welding by a specially designed tool. The cladding tube with thermocouples welded in place is then electron beam welded to a 30 in. tube extension which completes the tube and thermocouple assembly which is supplied by AI to Watlow.

The cladding thermocouple locations on all heaters are shown in Fig. D.10 and their relative locations in the rod bundle are shown in Fig. D.11. The angular positions (≤ 30 deg.) in Fig. D.11 are given with respect to the nearest line connecting the centers of adjacent heaters. As is evident from Fig. D.10, the -09 (dash number) heaters have 9 thermocouples welded to the inner cladding wall. Heater numbers H-1 and H-4 in Fig. D.11 are -09 heaters; however, since each heater had one inoperative thermocouple only 8 thermocouples are shown in Fig. D.11. The other heaters, as shown in Figs. D.10 and D.11, had either 2 or 3 cladding thermocouples. The thermocouple locations on heater numbers H-1 through H-7 were chosen to give an adequate mapping of the cladding circumferential temperature distribution. The cladding thermocouples on heater numbers H-8 through H-19 were used for symmetry checks and outer edge coolant channel effects, such as hot spots.

The inner seven rods also had thermocouples installed in the center of the heating coil at the same axial location as the cladding thermocouples. The center thermocouples were used to monitor the maximum heater temperatures and to determine approximately the effective MgO thermal conductivity. Besides providing a check on the uniformity of the heaters, the conductivity determination was useful in providing the k_{MgO}/k_w ratio which was used in the theoretical predictions (Figs. B.4 through B.6). As previously mentioned, the k_F/k_w ratio influences the azimuthal temperature and heat flux gradients.

The test section inlet and outlet bulk coolant temperatures were measured by 1/6 in. O.D., chromel-alumel thermocouples. As is shown in Fig. D.4, four thermocouples were inserted in the inlet plenum, and two thermocouples were in the main flow line exiting from the test section. These thermocouples were immersed in the flow stream and inserted through 1/8 in. swagelock fittings.

HEAT TRANSFER TEST

(REVISED 5-3-71)

- HEATER TC
- IMMERSION TC
- DUMMY TC
- FILLER ROD

TR-653-360-001
Page 60

Fig. B.11 Relative thermocouple location in 19 rod bundle.

HEATER NO. (SEE ENR 757 920 001)

SPACER NO.

ANGULAR DEGREE FROM
CLOSEST CONTACT POINT

THERMOCOUPLE NO.

To determine the coolant temperature profile at the outlet of the rod bundle, chromel-alumel thermocouples, 1/16 in. O.D., were inserted at the outlet of selected individual triscusp coolant channels. Twelve coolant channels were monitored, and the locations in the rod bundle are shown in Fig. D.11. The thermocouples were pointed into and aligned parallel to the flow stream. They were held in place by 0.030 thick bands which were attached to the heaters. The axial position of the thermocouples, shown in Figs. D.4.4 and D.5, was at the beginning of the 1/2 in. O.D. heater section, which is 22 in. downstream from the end of the heated zone. Since it was desired for these thermocouples to record the average triscusp channel temperatures, the axial location was chosen to allow sufficient axial length for thorough mixing of the individual coolant channel temperature gradients established in the heated zone. In order to maintain flow symmetry, dummy wires, 1/16 in. O.D., were banded in the same manner at the same axial location and were located in coolant channels where no thermocouples existed. Figure D.12 shows the wiring penetrating the top end of the test section from cladding and coolant channel thermocouples.

The NaK flow rate was measured by a D.C. permanent magnet flowmeter. The flowmeter was calibrated in a NaK flowmeter calibration loop and is accurate to within 3%.

The power to each test rod is controlled by 19 separate variable transformers connected in series with the heaters. Figure D.13 shows the console containing the 19 variable transformers. The power to each heater element was measured by a precision ammeter and voltmeter which were calibrated. The accuracy of the power measurement is within 1%.

The emf output of the power controllers, flowmeter, and all test section thermocouples were recorded with a model 6687A Hewlett-Packard digital data

Figure D-12. Cladding and Immersion Thermocouple Wiring

7759-40970

Figure D-13. Power Controls for the 19 Test Rods

7759-40761

logging system. This is a 600 data channel system which records raw data (voltages) simultaneously on punched tape and printed paper tape. The maximum speed of the data logger is five points per second for D.C. measurements and one point per second for A.C. measurements. Approximately one minute was required to scan all the test data. The punched paper tape from the data logging system was fed into a GE-430 computer terminal. A computer program was used to convert the raw data into engineering units and give a preliminary reduction of the data in a few minutes.

All of the above instruments including thermocouples, flowmeter, voltmeters and ammeters are calibrated in accordance with the AI Standard Laboratory Procedures; all of the measurements are referenced to company primary standards, whose calibrations are traceable to the National Bureau of Standards.

E. EXPERIMENTAL PROCEDURE

Since most prior rod bundle liquid metal heat transfer experiments have produced anomalous results, it was the primary objective of the present study to produce dependable data obtained under carefully controlled conditions. Throughout the experiment, precautions were taken to insure that the quantity of dissolved oxides, impurities, and entrained gases were held to a minimum.

During inoperative periods, the loop was purged continuously with argon. Prior to filling with NaK, the loop was evacuated to an absolute pressure of 3×10^{-4} . With the NaK at room temperature, the loop was loaded by pressurizing the drain tank. In addition, the NaK was charged through a 5×10^{-4} micrometallic filter. The fill line in the drain tank was low enough and the residual NaK level was kept high enough to minimize the intake of gas or any floating oxides.

While the NaK was circulating through the loop, approximately 10% of the flow was continuously diverted through the cold trap. The minimum temperature of the cold trap was held to 100°F . By means of the special sampling section, NaK samples were taken periodically throughout the test. Chemical analysis verified that the O_2 concentration was held to 5 ppm.

All thermocouples including heater element cladding and centerline, test section inlet and outlet, and individual coolant channels were checked periodically throughout the test, while the NaK was circulated isothermally at various temperatures. Maximum discrepancy between any two thermocouples was $< 2^{\circ}\text{F}$. A.C. noise pickup by the thermocouples was determined to be negligible. The data logging system was equipped with special devices to filter out any A.C. noise pickup.

The resistances of the heater elements were checked periodically throughout the test. The calibration of the precision ammeters and voltmeters, which were used for measuring the power input to all 19 test rods, were also checked from time to time during the investigation.

Prior to making a run, the flow rate was set; the test rods were adjusted to the same power level within 1%; and the test section inlet coolant temperature was allowed to come to a predetermined equilibrium value within 1°F. Before taking data, sufficient time was allowed to reach steady state conditions. Although the time to reach equilibrium depended on flow rate and temperature, it was usually less than 30 minutes. To verify that the consistency of the data was good, several runs were made at any one particular set of the above conditions before going on to the next set. In addition, after proceeding through the complete range of conditions, many repeat runs were made.

The scope of the experimental study is presented in Table II. To determine the extent of thermal communication in tightly packed rod arrays, asymmetric heating tests were also performed. These consisted of reducing the power supplied to individual heaters in order to create coolant-to-coolant channel temperature differences. Low flow rate tests aimed at determining the transition Reynolds number for low P/D ratios were also conducted.

The NaK was circulated continuously through the loop even when no power was supplied to the heater elements or when testing was not taking place, such as nights and weekends. The NaK flow was stopped only when it came time to change from the P/D = 1.00 test configuration to the P/D = 1.05 test configuration. At that time, the NaK was dumped at 400°F by pressurizing the surge tank. The NaK was stored in the sealed drain tank until it was time to load the loop for the next test configuration.

The physical property values of NaK-78, which were used in the calculations and correlations to follow, were taken from the Sodium-NaK Supplement of the Liquid-Metals Handbook, Ref. 18.

TABLE II

Scope of the Experimental Study

1. NaK temperature range, °F	400 to 900
2. Heat flux range, Btu/hr-ft ²	27,800 to 111,300
3. Reynolds number range	5,000 to 30,000
4. Prandtl number range	0.0061 to 0.0115
5. Peclet number range	35 to 250
6. Flow direction	Vertically upward

F. SINGLE HEATER CALIBRATION TESTS

Before proceeding to a discussion of the 19-rod bundle experimental results, heat transfer tests which were conducted with a single test rod will be described. Referring to Fig. D.11, the tests were performed on the center heater (H-1) under conditions of: a) annular flow of liquid NaK, and b) pool boiling of water. The purpose of these tests was to calibrate the heater and thermocouple (cladding and centerline) assembly at power under well described heat transfer conditions. More specifically, the test objectives were to determine quantitatively:

- 1) The extent of any circumferential temperature gradients due to heater fabrication abnormalities.
- 2) The ability of the cladding thermocouples to record the true inside wall temperature.
- 3) The effective MgO thermal conductivity for various heater power levels.

By testing the heater element under symmetrical flow and heat transfer conditions, the extent of any "manufactured" azimuthal temperature gradients can be determined. These gradients would exist for test rod nonuniformities, such as, electric coil being off center, variation in MgO compactness, and different effective radial locations of the inner cladding thermocouples.

Since one of the primary objectives of the 19-rod bundle tests is to determine the circumferential temperature gradient caused by tight rod packing, any manufactured azimuthal gradients must be determined.

Although the cladding thermocouples are welded to the inside wall, it was suspected that they would not record the true inner cladding wall temper-

ature. This stems from the fact that the thermocouples have an effective diameter of 10-mils, and there exists a large radial temperature gradient ($\approx 4^{\circ}\text{F}/\text{mil}$) through the MgO . Therefore, if the effective thermocouple radial location is a few mils into the MgO , this would correspond to an appreciable temperature difference from the inner cladding wall. This temperature difference would have to be known in order to determine the film coefficient for the 19-rod bundle tests. By taking measurements under heat transfer conditions where film coefficient correlations have been firmly established, the effective radial locations of the cladding thermocouples can be found, and temperature correction factors determined.

The importance of the last objective of the calibration tests is due to the dependence of the test rod circumferential temperature and heat flux gradients on the MgO thermal conductivity. To properly compare theoretical and experimental results, the MgO conductivity should be determined. Also, by knowing the effective MgO thermal conductivity, it insures that the heater element is tested within safe operating limits.

F.1 Description of NaK Calibration Tests

The center heater element (H-1) was tested under conditions of NaK annular flow. A specially designed NaK loop, which is used for calibrating flowmeters, was used and is shown in Fig. F.1. A schematic flowsheet and isometric drawing of the loop are shown in Figs. F.2 and F.3. The loop is capable of 1300°F operation with a maximum flow rate of approximately 10 gpm. The test section, shown in Fig. F.4, consisted of a 2 in., schedule 10, SS pipe into which the test rod was concentrically located. The loop and test section were insulated with at least 3 in. of fiberfrax and 3 in. of asbestos insulation. A cold trap, which was used to remove impurities from the loop, kept

Figure F-1. NaK Flowmeter Calibration Loop

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Fig.F.2 NAK FLOW METER CALIB TEST LOOP

Figure F.3 Isometric drawing of NaK flowmeter calibration loop.

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NaK LOOP

DESIGNED FOR NaK
MAX. TEMP. = 1400 °F
MAX. FLOW = 30 gpm @ 14.5 psi
MAX. BEARING CAPACITY = 500 PSI
PUMP & BEARING TANK VOLUME = 200 GALLONS
EXPANSION & CONTRACTION TANK VOLUME = 200 GALLONS
ALL LOOP PIPING IS INSULATED WITH 2 INCHES THERMAL INSULATION

NOTICE	
NaK FLOWMETER CALIBRATION LOOP - ISOMETRIC	
EXN76042310	

Figure F.4 Test section for NaK calibration tests.

Z.H. HEATER TEST

the oxide content below 10 ppm.

Power to the heater element was controlled by a variable transformer and was measured by a calibrated precision ammeter and voltmeter. Up to 4 Kw of power was supplied to the test rod, and the inlet NaK temperature was varied over the range of 400°F to 800°F. These power and temperature ranges are characteristic of those used for the 19-rod bundle tests. The NaK flow rate was measured by a permanent magnet flowmeter and was held constant at 10 gpm. The temperatures at the inlet and outlet of the test section were measured by calibrated thermocouples immersed in the fluid stream. These thermocouples, as well as the test rod centerline and nine cladding thermocouples, were recorded on a digital data logging system. All thermocouples were checked periodically by isothermal operation of the loop at various temperatures.

F.2 Description of Water Calibration Tests

To provide a second and completely independent calibration test, the center heater (H-1) was tested in water under steady-state pool boiling conditions. The test rod was installed vertically in a 50 gallon drum of distilled water. Special blanket heaters and insulation were used to maintain the water at saturation temperature. The power to the test rod was adjusted until steady-state pool boiling of the water existed. The heater power level ranged from 1 to 4 Kw and was controlled and measured in the same manner as in the NaK calibration test. Also, for each power setting, the same data logging system was used to record all temperatures including the bulk coolant, heater element cladding and centerline.

F.3 Results of Calibration Tests

For the water and NaK calibration tests, Figs. F.5 and F.6 show the circumferential variation of the cladding thermocouple readings above the bulk coolant temperature for various power levels. The thermocouple located at

Fig. F.5 CLADDING TEMPERATURE
 VARIATION FROM WATER
 POOL BOILING CALIBRATION
 TESTS

T/C LOCATIONS

Fig. F.6 CLADDING TEMPERATURE VARIATION FROM
 NaK ANNULAR FLOW CALIBRATION TESTS

$\theta = 270^\circ$ (T/C No. 17, Fig. D.10) was found to be inoperative. As is evident from these plots, there is very little temperature variation, and any "manufactured" azimuthal temperature gradient can be considered negligible. This indicates that the nichrome heater coil is well centered; the MgO is compacted uniformly; and the effective radial locations of the cladding thermocouples are approximately the same.

To determine if the test rod cladding thermocouples were recording the true inside wall temperature, the following relation was employed

$$T'_{w_i} = T_b + \Delta T'_f + \Delta T'_w \quad (7)$$

In Eq. (7), the primes indicate quantities which are calculated. For the NaK tests, T_b is the bulk coolant temperature at the axial location of the cladding thermocouples and is determined from measurements of the inlet and outlet NaK temperatures; for the water tests, T_b is the measured saturation temperature. The temperature drop across the cladding wall ($\Delta T'_w$) was calculated from

$$\Delta T'_w = \frac{W_f \ln\left(\frac{R}{r_3}\right)}{2\pi L_H k_w} \quad (8)$$

To determine the film temperature drop in the NaK calibration tests, the following relation developed by Dwyer¹⁹ and verified experimentally in Ref. 16 for the case of liquid metals flowing turbulently through concentric annuli where the heat flux is from the inner wall only was used

$$Nu_\infty = \alpha + \beta (\bar{\psi} Pr)^\gamma \quad (9)$$

where

$$\alpha = 4.82 + 0.697(r_o/r_i)$$

$$\beta = 0.0222$$

$$\gamma = 0.758(r_o/r_i)^{0.053}$$

The variable $\bar{\psi}$ in Eq. (9) was evaluated from

$$\bar{\psi} = 1 - \frac{1.82}{P_r (E_M/N)^{1.4}}_{\text{MAX.}}$$

with the quantity, $(E_M/N)_{\text{MAX.}}$, determined from the results of Dwyer²⁰. The axial position at which the inner cladding wall temperature was calculated corresponds to $z/D_h = 5.0$. At this axial location, the temperature profiles are not fully developed. To correct Eq. (9) for this, the experimental study of Duchatelle and Vautrey²¹ for turbulent flow of NaK between parallel plates with unilateral heat transfer was used. For simultaneous development of the velocity and temperature profiles, the ratio of the film coefficient at the axial position ($z/D_h = 5.0$) of interest to the fully established value corresponds to

$$\frac{Nu_z}{Nu_\infty} = \frac{h_z}{h_\infty} = 1.3$$

From this result and Eq. (9), the film temperature drop for the NaK calibration test was determined from

$$\Delta T'_f = \frac{W_r}{\pi D L_H h_z} \quad (10)$$

To determine the film temperature drop in the water calibration tests, Rohsenow's pool boiling correlation was used. For the fluid-surface combination of water and stainless steel, the following relation recommended by Tong²² was used

$$\Delta T'_F = \frac{0.014 H_{F2}}{C_1} \left[\frac{W_r}{\pi D L_H \mu_2 H_{F2}} \sqrt{\frac{q_o \sigma}{2(p_p - p_v)}} \right]^{1/3} P_{r_2}^{1/7} \quad (11)$$

For various rod power levels, Fig. F.7 shows the calculated variation of the film temperature drops, Eqs. (10) and (11), and the cladding wall temperature drop, Eq. (8). From these results and the bulk coolant temperature, the calculated cladding inside wall temperature (T'_{w_1}) for the NaK and water calibration tests can be determined from Eq. (7).

To determine how closely T'_{w_1} compares with the measured value, the follow correction factor was defined.

$$\Delta T_{\text{corr.}} = T'_{w_1} - T_{w_1} \quad (12)$$

where T_{w_1} is the average value of the inside cladding thermocouple readings, Figs. F.5 and F.6. For the NaK and water calibration tests, Fig. F.8 shows the variation of the correction factor, Eq. (12), with heater power. For both tests, the results indicate that the cladding thermocouples are recording temperatures which are greater than the true inner wall temperature, T'_{w_1} as determined by Eq. (7). This is not surprising in view of the thickness of the thermocouples, 0.010 in., and the large radial temperature gradient ($\approx 4^\circ\text{F}/\text{mil}$) in the MgO. Also, for the two independent calibration tests, the $\Delta T_{\text{corr.}}$ results in Fig. F.8 compare favorably, especially for the higher power levels. At the lower power levels, the discrepancy can be attributed to large differences in absolute MgO temperatures. In comparing theoretical and experimental results for the 19-rod bundle tests, the correction factors of Fig. F.8 for NaK were used to correct the experimental results for the inside cladding azimuthal temperature distribution and the rod-average film coefficient. The correction factors for the NaK calibration tests were used because these tests

Fig. 5.7 FILM TEMPERATURE DROP FOR
NaK ANNULAR FLOW TEST AND
H₂O POOL BOILING TEST

Fig. E.8 CENTER HEATER CALIBRATION TESTS -
TEMPERATURE CORRECTION VS
POWER FOR INSIDE CLADDING
THERMOCOUPLES

were conducted over the same temperature range as used in the rod bundle studies

For the various operating conditions examined in the NaK and water calibration tests, the test rod centerline thermocouple was continuously recorded. From the heater element centerline temperature and the average inside cladding wall temperature, the effective MgO thermal conductivity was calculated from

$$k_{MgO} = \frac{W_r \ln(r_3/r_2)}{2\pi L_H \Delta T_{MgO}} \quad (13)$$

For the NaK and water calibration tests, the variation of k_{MgO} with heater power is shown in Fig. F.9. As previously mentioned for closely packed rod arrays, the azimuthal heat transfer gradients are dependent upon k_{MgO} , and the values shown in Fig. F.9 for NaK were used in theoretical predictions of the cladding circumferential temperature distribution and the rod-average film coefficient (see Figs. B.4 through B.6). The difference between the k_{MgO} values for the NaK and water tests, as shown in Fig. F.9, can be attributed to different absolute MgO temperatures.

Fig. F.9 EFFECTIVE MgO THERMAL CONDUCTIVITY VARIATION WITH ROD POWER FROM CALIBRATION TESTS ON H-1

G. EXPERIMENTAL RESULTS

For the 19-rod bundle configurations with $P/D = 1.00$ and 1.05 , experimental heat transfer results will be presented for the cladding circumferential temperature distribution and the rod-average film coefficient. The local azimuthal temperature distributions which will be presented are those recorded by thermocouples welded to the inner cladding wall and have not been corrected for using the results of Fig. F.8 for the single heater calibration tests. The correction factors of Fig. F.8 were used only when comparison was made between theory and experiment for the inner cladding temperature distribution and the rod-average heat transfer coefficient.

The cladding thermocouple locations relative to each other in the rod bundle, as shown in Fig. D.11, were determined in the following manner. As previously mentioned, the thermocouples on any one test rod were circumferentially held in place during welding to the inner cladding wall by a specially designed tool, and the relative angle between thermocouples on the same heater element, as shown in Fig. D.11, is accurate to within 2 degrees. A scribe line was placed on the outer surface of each test rod at a known circumferential location from one of the inner cladding thermocouples. With the use of a template, the scribe lines on each test rod were then located with respect to a fixed reference line scribed on the test section cylindrical shroud. During assembly of the 19-rod bundle, it was possible that the rods were inadvertently rotated causing the thermocouples to be moved from their intended positions; therefore, a test was devised to locate the cladding thermocouples on all test rods.

Since the relative angles between thermocouples on any one test rod are known accurately, it remains to determine their location with respect to

fixed reference points in the rod bundle. For the $P/D = 1.00$ test configuration, the reference points were chosen to be the contact points ($\theta = 0^\circ$) between adjacent heater elements. The test consisted of supplying power to only one test rod in the 19-rod bundle and comparing the magnitude of the temperature response of thermocouples on adjacent rods. Thermocouples nearest the operating heater contact points will respond the highest in temperature; thermocouples furthest from the contact points will respond the least. By operating all 19 heater elements separately and comparing the response of the cladding thermocouples, it was determined that the azimuthal locations of these thermocouples could be located within 5 degrees from the nearest contact point. The final cladding thermocouple locations which were arrived at by means of these tests are shown in Fig. D.11. These locations were within 10 degrees of the predetermined design locations.

Since the test rods are arranged on hexagonal arrays, the circumferential temperature gradients are repeated every 60 degrees and are referred to as the "60 temperature ripple" in the figures which follow. Since the temperature distributions are symmetrical with respect to each 30 degree interval (see Fig. A.2), the angle θ in the following figures lies in the range $0^\circ \leq \theta \leq 30^\circ$ and is measured from the cladding thermocouple to the nearest line connecting the centers of adjacent elements.

Circumferential temperature distributions will be presented separately for heater numbers H-1 and H-4 (Fig. D.11) which had 8 operative cladding thermocouples. In addition, a combined plot was made showing the temperature distribution as recorded by thermocouples on the inner seven heaters (H-1 through H-7).

Table III presents a list of the test parameters - heater power, flow rate, and inlet temperature - examined in the present study. For the above parameters, the corresponding heat fluxes, Reynolds numbers, and Peclet numbers are also listed.

GG.1 P/D = 1.00 Results

For heater power levels of 1.7, 2.4, 3.0, and 4.0 Kw; Figs. G.1 through GG.12 show the actual circumferential temperature distributions as measured by cladding thermocouples (T/Cs) on the inner seven test rods. The T/C numbers are shown on these plots, and their relative location in the 19-rod bundle can easily be found by referring to Fig. D.11. For each of the four power levels, three plots are presented. The first plot shows the temperatures as recorded by T/Cs on H-1; the second plot shows T/C measurements on H-4; and the third plot shows temperatures as recorded by T/Cs on H-1 through H-7. It was found that T/Cs on H-3 and H-5 were giving erroneous readings and were not plotted.

For the temperature distributions as recorded by T/Cs on a single heater, H-1 or H-4, there is very little data scatter; however, for the combined plots showing the T/C measurements of the inner seven heaters, more data scatter is evident. The increased data scatter for the combined plots can be attributed to fabrication non-uniformities of the heater elements, inaccuracy of T/C locations to within 5 degrees, and small temperature differences for the individual tricuspid coolant channels. Also, the data scatter increases with power level because of increasing axial temperature rise of the coolant. It should be noted, however, that the data scatter evident in Figs. G.1 through GG.12 is really not large for this type of investigation.

TABLE III
Test Parameters

Heater Power (Kw)	Heat Flux (Btu/hr-ft ²)	Inlet Temperature (°F)	Flow Rate (gpm)	Reynolds No.	Peclet No.
1.0	27,800	400	5.0	5,000	35
1.7	47,300	600	7.5	7,000	55
2.4	66,800	800	10	10,000	75
3.0	83,500		15	15,000	115
4.0	111,300		20	19,000	150
			26	25,000	200
			31	30,000	250

Fig. G.1 CLADDING CIRCUMFERENTIAL TEMPERATURE DISTRIBUTION

DP 18 B, 4/25/71

1.7 KW, 600°F, 267 PM

HEATER #1 — A

$P/D = 1.00$

Fig. G.2 CLADDING CIRCUMFERENTIAL TEMPERATURE DISTRIBUTION

DP 18 B 4/20/71

1.7 KW, 600°F, 269 RPM

HEATER # 4 — ○

P/D = 1.00

Fig. G.3 CLADDING CIRCUMFERENTIAL TEMPERATURE DISTRIBUTION

DP 188 4/28/71
1.7 KW, 600 PS, 26 rpm

P/D = 1.00

HEATER #1 Δ
HEATER #4 $\circ$
HEATER #2 $\square$ 60.7 X

Fig G.4 CLADDING CIRCUMFERENTIAL TEMPERATURE DISTRIBUTION

P/D = 1.00

HEATER #1

$T_c(\theta)$, deg.

$T_b = 661^\circ F$

θ , deg.

Fig. G.5 CLADDING CIRCUMFERENTIAL TEMPERATURE DISTRIBUTION

DATA PT 19-A 4-20-71
2.4 KW 600°F 26.6 AM

HEATER # 4

P/D = 1.00

Fig. G.6 COOLING CIRCUMFERENTIAL TEMPERATURE DISTRIBUTION

DATA PT 18-A - 4-21-71
2.4 KW 600° + 26 GPM

$P/D = 1.00$

HEATER #1 Δ
HEATER #4 $\circ$
HEATERS #2 & 6 $\square$ $\times$

Fig. 3.1 CLADDING CIRCUMFERENTIAL TEMPERATURE DISTRIBUTION

DATA PT 102 4-22-71
3.0 KW 600/ 26 GPM

HEATER # 1

P/D = 1.00

Fig 3.8 CLADDING CIRCUMFERENTIAL TEMPERATURE DISTRIBUTION
 DATA Pt. 20-C 4-22-71
 3.0 KW 600°F 26 GPM

HEATER # 4

$P/D = 1.00$

$T_{w, \text{cl}}, ^\circ\text{F}$

Fig. 3.9 CLADDING CIRCUMFERENTIAL TEMPERATURE DISTRIBUTION

DATA TP 20 & 4-22-91
3.0 KW 600°F 26 GPM

P/D = 1.00

HEATER # 1A

HEATER # 2C

SATIN # 20607X

Fig. 3.10 CHODDING CIRCUMFERENTIAL TEMPERATURE DISTRIBUTION

DATA PT 21 K 4-23-71
4.0 KW 400°F 26 GPM

HEATER #1 Δ

$P/D = 1.00$

$T_b = 427^{\circ}\text{F}$

Fig. 3.11. CIRCUMFERENTIAL TEMPERATURE DISTRIBUTION

DATA PT 21 K 4-28-71
4.0 KW 400°F 26 GPM.

HEATER # 4 @

$P/D = 1.00$

Fig. G.12 CLADDING CIRCUMFERENTIAL TEMPERATURE DISTRIBUTION

DATA PT 21K 4-23-74
4.0 KW 400°F 26 GPM

P/D = 1.00

HEATER #1 Δ
HEATER #4 ○
HEATERS #2 □ 6 ○ 7 X

$T_b = 489^\circ\text{F}$

To determine the Peclet number dependence of the circumferential temperature gradients, Figs. G.13 and G.14 present data for the local cladding temperatures above the bulk coolant temperature for various flow rates. As is evident from the data, there is essentially no Pe dependence over the range investigated.

The bulk coolant temperature (T_b) which is subtracted from the local cladding temperatures in Figs. G.13 and G.14 and in subsequent plots is the average NaK temperature at the cladding thermocouple plane, $z_H/L_H = 0.80$. T_b is determined from the average inlet temperature and the average of the individual outlet coolant channel temperatures surrounding the inner seven heaters, TC numbers 80 through 89, Fig. D.11. The coolant channel temperatures as recorded by T/C numbers 80 through 89 were within 3% of being uniform. Heat balances made for various conditions (power, flow rate, and temperature) indicated that test section heat losses were $< 1\%$.

Figures G.15 through G.24 present the local variation of the quantity $(T_{W_i} - T_b)$ for heater power levels of 1.0, 1.7, 2.4, 3.0 and 4.0 Kw. For each power level, two sets of data are presented; the first plot shows the temperature distribution as recorded by T/Cs on the center heater, and the second data set is a combined plot of T/C measurements on the inner seven test rods. For the center test rod, Fig. G.25 presents a summary of the azimuthal temperature distributions for each power level examined in the study. It can be seen from Fig. G.25 that the maximum circumferential temperature difference $[T_{W_i}(\theta = 0^\circ) - T_{W_i}(\theta = 30^\circ)]$ is essentially linear with power, except for a small discrepancy due to changing MgO thermal conductivity with power (see Fig. F.9).

Fig. G.13

60 RIPPLE - CENTER HEATER

170 KW
 $P/D = 1.00$

FLOW RATE - GPM	PC
26 GPM	205
20 GPM	156
15 GPM	117
10 GPM	78

Fig. G.14

60 RIPPLE - CENTER HEATER

2.40 KW
P/D = 1.00

FLOW RATE	Pe
26 GPM	206
20 GPM	156
15 GPM	117

Fig. G.15 60 TEMPERATURE RIPPLE

1000 6000 20000

P/D = 100

HEATER #1 A

$T_{\text{avg}}(\theta) - T_{\text{avg}}(0)$

$\theta, \text{deg.}$

Fig. G.16 60 TEMPERATURE RIPLE

Fig. G.17 60 TEMPERATURE RIPPLE

DATA AT 10.0 2-28-71
17 kW 600°f 26.608

HEATER #1 A

P/D = 1.00

$T_w(\theta) - T_b, ^\circ F$

140
120
100
80
60
40
20
0

5

10

15

20

25

30

θ, deg

Fig. G.18 60 TEMPERATURE RIPPLE

DATA PT. 188 4-20-71
1.7 KW 600°/ 26 GPM

P/D=1.00

HEATER 1 Δ
HEATER 4 O
HEATER 2 E 6 Q 7 X

Fig. G.19 60° TEMPERATURE RIPPLE

DP-19A
2.4 MA
26.7 PM
600 °F

HEATER #1

$P/D = 1.00$

Fig G.20 60 TEMPERATURE RIPPLE

DATA PT 100 4-21-73
2.4 KW 600°f 26 GPM

P/D = 1.00

HEATER #1 Δ
HEATER #4 ○
HEATER #2 □ 0607X

Fig. G.21 6.0 TEMPERATURE RIPPLE

DATA: T 20.3 4-21-7
3.0 kW 600°/ 26.5°/

HEAT-UP #1 A

P/D = 1.00

Fig. G.22 .60 TEMPERATURE RIPPLE

DATA, PT 200 4-28-71
3.0 MW 600% 26.6M

P/D = 1.00

HEATER #1 A
HEATER #4 B
HEATER #2 C G O 7X

Fig. 3.23 60 TEMPERATURE RIPPLE

DP-21K
4.0 KW
750 RPM
400 °F

HEATER #1 Δ

$P/D=1.00$

Fig. G.24 60 TEMPERATURE RIPPLE

DATA PT 21 K 4-23-71

40 KW 400°f 26 GPM

$P/D = 1.00$

HEATER #1

HEATER #4

HEATER #2 60.7 X

Fig. G.25

60 RIPPLE - CENTER HEATER

26 GPM

$P/D = 1.00$

HEATER POWER (Kw)

- $\triangle$ 1.0
- Δ 1.7
- $\diamond$ 2.4
- $\circ$ 3.0
- $\square$ 4.0

$T_w(\theta) - T_b$

θ , degrees

In order to compare experimental results with theoretical predictions for the inside cladding circumferential temperature distribution (see Figs. B.4 and B.5), the center heater T/C measurements, Figs. G.15 through G.24, had to be corrected for not measuring the true inner cladding wall temperature. As explained previously, the temperature correction factors were determined by calibration tests on the center heater (H-1) and are shown in Fig. F.8. Since the NaK calibration tests were conducted over the same temperature range as the rod bundle studies, the correction factors for NaK in Fig. F.8 were used.

For various power levels, Figs. G.26 through G.29 show comparisons of theoretical and experimental results for the inside cladding azimuthal temperature distribution. The "experimentally corrected" results were determined from the uncorrected experimental results, Fig. G.25, for the center test rod and the correction factors for NaK, Fig. F.8., obtained with the same rod. As is evident from Figs. G.26 through G.29, good agreement is obtained between the predicted and the experimentally corrected results.

From the theoretical results presented in Figs. B.4, B.5 and Ref. 1; it can be shown that the average inside cladding wall temperature ($\bar{T}_{w_i}$) is approximately equal to the local temperature at $\theta = 15^\circ$, i.e.,

$$\bar{T}_{w_i} = \frac{1}{\pi/6} \int_0^{\pi/6} T_{w_i}(\theta) d\theta \approx T_{w_i}(\theta = 15^\circ) \quad (14)$$

This relationship has also been predicted to hold true for the outside cladding wall temperature distribution $[T_{w_o}(\theta)]$. Equation (14) has been verified experimentally from the results of Figs. G.15 through G.24. Figures G.26 through G.29 show that good agreement exists between the predicted and experimentally corrected values for the average inside cladding wall temper-

Fig. G.26. 60 RIPPLE - CENTER HEATER
COMPARISON OF THEORY
WITH EXPERIMENT .

1.70 KW
26 gpm
P/D = 1.00

Fig G.27 60 RIPPLE - CENTER HEATER
COMPARISON OF THEORY WITH
EXPERIMENT

Fig. G.28 60 RIPPLE - CENTER HEATER
COMPARISON OF THEORY
WITH EXPERIMENT

Fig. G.29 60 RIPPLE - CENTER HEATER
COMPARISON OF THEORY
WITH EXPERIMENT

4.0 KW
26 gpm

$P/D = 1.00$

ature $[T_{W_1}(\theta=15^\circ)]$. As will be shown later, the good agreement for $\bar{T}_{W_1}$ has an important bearing on the results for the rod-average heat transfer coefficient.

Referring to Figs. G.26 through G.29, the maximum circumferential temperature difference $[T_{W_1}(\theta=0^\circ) - T_{W_1}(\theta=30^\circ)]$, as measured by the cladding thermocouples, is less than the predicted value. This can be explained by examining the attenuation of the azimuthal temperature gradients in the MgO. Calculations have been made for the variation of the quantity $[T(\theta=0^\circ) - T(\theta=30^\circ)]$ with distance into the MgO and are shown in Fig. G.30. By comparing the predicted maximum azimuthal temperature differences in Fig. G.30 to experimental values in Figs. G.26 through G.29, good agreement is obtained at a distance corresponding to approximately 5 mils into the MgO. This is not surprising in view of the fact that 5-mils corresponds to one-half of the cladding T/C thickness. For various power levels, Figs. G.31 through G.34 show calculations of the local circumferential temperature distributions for various heater element radial positions. These figures also show the measured local temperatures from T/Cs on the inner seven test rods taken from Figs. G.15 through G.24. As is evident from Figs. G.31 through G.34, good agreement between theory and experiment for the local temperature gradient is obtained at a radial location corresponding to 5-mils into the MgO. These results indicate that the cladding T/Cs for the inner seven test rods are recording temperatures which correspond to approximately 5-mils into the MgO and give further support to the correction factor results shown in Fig. F.8 for the center heater.

Fig. G.30 MAXIMUM AZIMUTHAL TEMPERATURE
 DIFFERENCE VS. RADIAL LOCATION IN MgO

$P/D = 1.00$

$[T(\theta=0^\circ) - T(\theta=30^\circ)]$

°F

HEATER POWER (KW)

4.00
 3.00
 2.40
 1.70

DISTANCE INTO MgO, MILS

Fig. G.31 6θ TEMPERATURE RIPPLE
 COMPARISON OF THEORY
 WITH EXPERIMENT

Fig. G.32 60 TEMPERATURE RIPPLE
COMPARISON OF THEORY
WITH EXPERIMENT

Fig. G.33 60 TEMPERATURE RIPPLE
COMPARISON OF THEORY
WITH EXPERIMENT

Fig. G.34 60 TEMPERATURE RIPPLE
COMPARISON OF THEORY
WITH EXPERIMENT

4.0 Kw
269pm
P/D = 1.00

The experimental values for the rod-average film coefficient were determined for the center heater (H-1) in the following manner. From the experimentally corrected results such as those shown in Figs. G.26 through G.29, for the inside cladding circumferential temperature distribution, the average outer clad temperature is found from

$$\bar{T}_{w_o} = \bar{T}_{w_i} - \Delta T_w' \quad (15)$$

where $\Delta T_w'$ and $\bar{T}_{w_i}$ are calculated from Eqs. (8) and (14). From the measured power to the center test rod and the bulk coolant temperature at $z_H/L_H = 0.80$, the rod average film coefficient is determined by

$$\bar{h} = \frac{W_r}{\pi D L_H (\bar{T}_{w_o} - T_b)} \quad (16)$$

The experimental film coefficient results are shown in Fig. G.35 in dimensionless form of Nusselt number vs Peclet number. The predicted Nu value (see Fig. B.6) assuming Pe independence is also shown in Fig. G.35 for comparison. The agreement between theory and experiment is excellent. In the flow range investigated, these experimental results confirm that the Nu values are independent of Pe, unlike the results of Subbotin et al.^{14,15}. The predicted value of Nu=0.560 corresponds to a heat transfer coefficient of $\bar{h} = 1750 \text{ Btu/hr-ft}^2\text{-}^\circ\text{F}$.

Fig. B.35 COMPARISON OF THEORY WITH
EXPERIMENT FOR NUSSELT NO.
VS. PECELET NO. FOR CENTER HEATER

G.2 P/D = 1.05 Results

For heater power levels of 1.0, 1.7, 2.4, 3.0, and 4.0 Kw; Figs. G.36 through G.45 show the local variation of the quantity $(T_{w_1} - T_b)$. Two figures are presented for each power level; the first figure shows separately the temperatures as measured on heater numbers 1 and 4, and the second figure shows the cladding temperatures as measured by T/Cs on the inner seven test rods. As is evident from Figs. G.36 through G.45, there is essentially no circumferential temperature gradient for each power level examined in the study. These results are in contrast to the P/D = 1.00 results shown in Figs. G.15 through G.24. As was the case for P/D = 1.00 results, very little data scatter exists for temperatures as recorded by T/Cs on a single heater (H-1 or H-4), while more scatter exists when comparing T/C measurements on the inner seven rods. The reason for the increased data scatter from T/Cs on the inner seven rods is as explained before for the P/D = 1.00 results.

For the center test rod (H-1), Fig. G.46 shows a summary of the cladding temperature distributions for various power levels. The uniform temperature distributions shown in Fig. G.46 are markedly different from the large azimuthal gradients shown in Fig. G.25 for the P/D = 1.00 tests. Also, the results of Fig. G.46 are surprising in view of the theoretical predictions for P/D = 1.05, which are shown in Fig. B.5. Since the validity of the analytical model has been verified by the P/D = 1.00 tests, the experimental results of Fig. G.46 are suspected to be influenced by the presence of the 30-mil spacer rings, which are shown in Fig. D.8. As previously mentioned, the rings were used to maintain a symmetrically spaced rod bundle having a P/D = 1.05. A significant flow perturbation was caused by the spacer rings, evidenced by the fact that 75% of the total rod bundle pressure drop could be

Fig. G.36 CLADDING TEMPERATURE DISTRIBUTION

Fig. G.37 CLADDING TEMPERATURE DISTRIBUTION

Fig. G.38 CLADDING TEMPERATURE DISTRIBUTION

DP 200 D
1.7 KW
25 GPM
600 °F

P/D=1.05

HEATER #4

HEATER #1

Fig. Q.39 GLASSING TEMPERATURE DISTRIBUTION
DP 200 D

1.7 KW

26 GPM

600 °F

PYD=1.05

Δ - HEATER #1

○ - HEATER #4

□ - HEATER #2

⊙ - HEATER #3

* - HEATER #7

$T_{wi}(\theta) = T_b$

FIG. 6.40 CLADDING TEMPERATURE DISTRIBUTION

P/D = 1.05

DP 221 C

2.4 KW

26 GPM

600 °F

○ - HEATER # 4

△ - HEATER # 1

$T_w(\theta) - T_b$

FIG. 6.10 CLADDING TEMPERATURE DISTRIBUTION

DP 201 C
2.4 KW
26 GPM
600 °F

PID = 1.05

- △ - HEATER #1
- - HEATER #4
- - HEATER #2
- ⊙ - HEATER #3
- x - HEATER #7

Fig. G.42 CLADDING TEMPERATURE DISTRIBUTION

Fig. G.43 CLADDING TEMPERATURE DISTRIBUTION

DP 203 C
3.0 KN
26 5 PM
400 °F

P/D = 1.05

Δ - HEATER # 1
○ - HEATER # 4
□ - HEATER # 2
⊙ - HEATER # 3
X - HEATER # 7

Fig. G.44 CLADDING TEMPERATURE DISTRIBUTION

FIG. G.45 CLADDING TEMPERATURE DISTRIBUTION

DP 204 D
4.0 KW
26 GPM
400° F

P/D = 1.05

Fig. G.46 CENTER HEATER CLADDING TEMPERATURE DISTRIBUTIONS

26 gpm

P/D = 1.05

HEATER POWER (KW)

△ 1.0

△ 1.7

◇ 2.4

○ 3.0

□ 4.0

$T(\theta) - T_0$
°C

80

60

40

20

0

0

5

10

15

20

25

30

θ , degrees

HEATER #1

attributed to the spacers. For closely spaced ($P/D < 1.10$) rod bundles, the results of Zhukov et al.²³ indicate that 60 to 100 hydraulic diameters may be required to establish fully-developed temperature profiles. The middle spacer rings, as shown in Fig. D.8, are only 30 hydraulic diameters from the axial plane of the cladding thermocouples; therefore, the uniformity of the cladding temperature distributions, as shown in Figs. G.36 through G.45, is believed to be the result of the middle spacer rings. Apparently, a significant increase in the fluid turbulence intensity results downstream of the spacers rings. This causes appreciable intra-channel mixing which negates any coolant azimuthal temperature gradient.

Figures G.47 and G.48 show the local variation of $[T_{w_i}(\theta) - T_b]$ for various flow rates. Although there is no circumferential variation, the average value of $[T_{w_i}(\theta) - T_b]$ does show a Peclet number or flow dependence, unlike the results of Figs. G.13 and G.14 for the $P/D = 1.00$ tests. Over the flow range investigated, the results of Figs. G.47 and G.48 show approximately a 25% variation in the average cladding wall temperature. As will be shown later, the above flow dependence will be reflected in the results for the rod-average film coefficients. Although the flow dependence is not great, it does exist and most likely is due to a Reynolds number dependence of the intra-channel mixing, which is caused by the spacer rings.

Comparisons between theory and experiment for the inside cladding circumferential temperature distribution are shown in Figs. G.49 through G.52 for various power levels. The experimental results obtained from the inside cladding T/Cs on the center test rod, which were shown previously in Fig. G.46, were corrected from the experimental results of Fig. F.8. The theoretical results were presented earlier in Fig. B.5. For the magnitude of the

Fig. G.47 CLADDING TEMPERATURE DISTRIBUTIONS FOR VARIOUS FLOW RATES

CENTER-HEATER
2.4 KW
P/D = 1.05

$T(\theta) - T_p$
°F

FLOW RATE (GPM)

- ◇ - 15
- - 20
- - 26
- - 31

θ - DEGREES

Fig. G.48 CLADDING TEMPERATURE DISTRIBUTIONS FOR VARIOUS FLOW RATES

Fig. G-49 60 TEMPERATURE RIPPLE - CENTER HEATER
 COMPARISON OF THEORY WITH EXPERIMENT

$P/D = 1.05$
 1.7 KW
 76 gpm

Fig. G. 50 60 TEMPERATURE RIPPLE - CENTER HEATER
 COMPARISON OF THEORY WITH EXPERIMENT

$P/D = 1.05$
 2.4 KW
 269 RPM

Fig. G.51 60 TEMPERATURE RIPPLE - CENTER HEATER
COMPARISON OF THEORY WITH EXPERIMENT

$P/D = 1.05$

3.0 KW
26 gpm

Fig. G.52 60 TEMPERATURE RIPPLE - CENTER HEATER
COMPARISON OF THEORY WITH EXPERIMENT

$P/D = 1.05$
4.0 KW
26 gpm

azimuthal temperature gradients, poor agreement is seen to exist between theory and the "experimentally corrected" results; however, for the average inside cladding temperature, good agreement is found by noting that $\bar{T}_{W_1} \approx T_{W_1}(\theta=15^\circ)$. These results indicate that the presence of the spacer rings, while negating the circumferential temperature gradients, had little influence on the average cladding temperature. Apparently, the increase in the intra-channel turbulence intensity, which is caused by the spacers, is primarily in the azimuthal direction with little influence on the radial component.

The experimental values for the rod-average film coefficient were determined in the same manner as used for the $P/D=1.00$ tests. Equation (14) was used to determine the average inside cladding temperature from the "experimentally corrected" results obtained on the center test rod, such as those shown in Figs. G.49 through G.52. Equations (15) and (16) were then used to calculate the average outside cladding wall temperature ($\bar{T}_{W_0}$) and rod-average film coefficient ($\bar{h}$), respectively.

For the $P/D = 1.05$ test configuration, Fig. G.53 shows the experimental film coefficient results in dimensionless form of Nu vs Pe . The theoretical results (see Fig. B.6), assuming molecular conduction only, are shown in Fig. G.53 for comparison. For $Pe > 150$, the experimental values of Nu are 10% to 35% greater than the predicted values and show a slight Pe dependence, which also existed for the average inside cladding temperature, shown in Figs. G.47 and G.48. For $Pe < 150$, good agreement between the experimental and theoretical Nu values is shown in Fig. G.53. For this type of investigation, the agreement between the experimental and predicted Nu values is considered to be excellent over the full Pe range investigated. Although poor agreement was obtained for the azimuthal temperature gradients, the

Fig. G. 53

COMPARISON OF THEORY WITH EXPERIMENT
FOR NUSSLELT NO. VS. PECLET NO. FOR
CENTER HEATER

$P/D = 1.05$

NUSSLELT NO.

PREDICTED
(see Fig. B. 6)

HEATER POWER (KW)

- ◇ 1.0
- 1.7
- 2.4
- △ 3.0

10^1

PECLET NO.

measured average inside cladding temperatures were close to predicted. This explains the good agreement between the experimental and predicted Nu value shown in Fig. G.53. Therefore, the spacer rings, while negating the azimuthal temperature gradient, had little influence on the rod-average cladding temperature and film coefficient. The predicted value of $Nu = 2.74$ in Fig. G.53 corresponds to a film coefficient of $\bar{h} = 3930 \text{ Btu/hr-ft}^2\text{-}^\circ\text{F}$.

G.3 Transition and Laminar Flow Tests

For the rod bundle configurations examined in the present study, an attempt was made to determine the transition Reynolds number and to acquire heat transfer data in the laminar flow regime. Tests were conducted at low coolant flow rates in the range of 2 to 10 gpm, which corresponded to a Re range of 2,000 to 10,000. In order to limit excessive coolant temperature rise, power levels in the range of 0.5 to 1.0 Kw per test rod were examined.

To determine the transition Re, temperature vs time recordings were obtained from selective thermocouples for the above range of operating conditions. For the rod bundle configuration having $P/D = 1.00$, Re values in the range of 2000 - 3000 were observed to cause large cladding temperature fluctuations, indicating that operation in the transition zone between turbulent and laminar flow was taking place. The magnitude of the cladding temperature fluctuations was approximately 50% of the average film temperature drop (ΔT_f) and occurred at a frequency of about 1 Hz. For the $P/D = 1.05$ test configuration, temperature fluctuations were not observed to occur for Re values down to 2000. Apparently, the presence of the spacer rings shifted the transition Re to a value below 2000.

Since it was not possible to operate at conditions corresponding to

$Re < 2,000$, laminar flow heat transfer data was not obtained. However, if it were possible to operate at flow rates corresponding to $Re < 2,000$, it would be very unlikely that reliable laminar flow heat transfer data could be obtained. Because of the necessity to operate at low values of flow rate and rod power, a large axial coolant temperature rise results in appreciable data scatter for the cladding temperature distribution. In most cases, the data scatter will be greater than the average film temperature drop corresponding to the low rod power levels.

G.4 Asymmetric Heating Tests

Tests were also conducted under conditions of asymmetric rod heating. For the 19-rod bundle configurations, selective heaters were turned off, while power to the other test rods was maintained. This, in effect, produced coolant-to-coolant channel temperature differences. For various asymmetric heating conditions, temperature distributions of the heater element cladding and outlet coolant channels were measured. The purpose of these tests was to determine the extent of thermal communication in closely packed rod bundles. More specifically, it was desired to determine the extent that conduction and inter-channel coolant mixing attenuate temperature gradients which were purposely created by asymmetric heating conditions.

With test rod number H-8 turned off (see Fig. D.11) and the other rods operating at 1.0 kw, the cladding temperature distributions $[T_{w_1}(\theta) - T_b]$ are shown in Figs. G.54 and G.55 for $P/D = 1.00$ and in Figs. G.56 and G.57 for $P/D = 1.05$. In the figures presented in Section G.4, T_b is obtained from an average of the four outlet T/Cs (T/C numbers 80 through 85, Fig. D.11) around the center test rod. By comparing the $P/D = 1.00$ results shown in

Fig. G.54 CLADDING TEMPERATURES - HEATER NO. 2 OFF

DATA PT 33A 5-10-71

1.0 KW 600°F HTR #8 OFF

264 ppm

HEATER #1 A

P/D = 1.00

S, deg.

Fig. G.55 CLADDING TEMPERATURES - HEATER NO. 8 OFF

DATA PT 33A 5-10-71

OKW 600°F HTG #8 OFF

FMW 26.3 GPM

P/D = 1.00

HEATER #1 S

HEATER #4 O

HEATERS 2, 3, 6, 7 X

Fig. G.56 CLADDING TEMPERATURES - HEATER NO. 8 OFF

P/D = 1.05

DP 214 A

1.0 KW

26 GPM

800 °F

(HEATER # 8 OFF)

Fig. G.57 CLADDING TEMPERATURES - HEATER NO. 8 OFF

DP 214 A
1.0 KW
26 GPM
500° F
(HTR. # 8 OFF)

P/D = 1.05

Δ - HEATER # 1
○ - HEATER # 4
□ - HEATER # 2
○ - HEATER # 6
X - HEATER # 7

Figs. G.54 and G.55 to those shown previously in Figs. G.15 and G.16 with all heaters operating at 1.0 Kw, it is evident that turning H-8 off had a negligible influence on the cladding T/C measurements on the inner seven test rods. For the $P/D = 1.05$ test configuration, the same conclusion is arrived at by comparing the results of Figs. G.56 and G.57 to those shown previously in Figs. G.36 and G.37. In the outer ring of test rods, heater number H-19 was also turned off, and the same results shown in Figs. G.54 through G.57 were found. It is apparent that the reduced coolant channel temperatures adjacent to a rod in the outer ring, which is turned off, have negligible influence on cladding temperatures as measured by T/Cs on the inner seven elements.

With heater numbers H-8 through H-19 turned off and the inner seven elements operating at 1.0 Kw, Figs. G.58 and G.59 show the center rod cladding temperature distributions for the $P/D = 1.00$ and 1.05 test configurations, respectively. By comparing these results to the results shown previously in Figs. G.15 and G.36 with all test rods operating at 1.0 Kw, it is seen that turning off the outer ring of test rods had a negligible influence on the center test rod cladding temperature distribution. For closely packed arrays, these results give evidence that a rod bundle having 7 heater elements surrounded by 12 dummy rods would be an adequate simulation of an infinite rod bundle for determining the center rod cladding temperature distribution and the average film coefficient.

For heater number H-2 turned off in the first ring (see Fig. D.11), the cladding temperature distributions are shown in Figs. G.60 and G.61 for $P/D = 1.00$ and in Figs. G.62 and G.63 for $P/D = 1.05$. By comparing the $P/D = 1.00$ results shown in Figs. G.60 and G.61 to those shown previously in

Fig. G.58 CLADDING TEMPERATURES - HEATER NOS. 8 THRU 19 OFF

DATA PT 33 R 5-11-71

1.0 KW 600°f HEATERS #8 THRU 19 OFF

2.69 PM

HEATER #1 Δ

P/D = 1.00

S, ft

Fig. G.59 CLADDING TEMPERATURES - HEATER NOS. 8 THRU 9 OFF

DP 214 V

1.0 KW

26 GPM

600 °F

(HTRS. # 8 THRU # 12 OFF)

P/D = 1.05

HEATER #:

Fig. G.60 CLADDING TEMPERATURES - HEATER NO. 2 OFF

DP 33E
1.0 KW
26.9 PPM
600 °F
(HTR. #2 OFF)
PID=1.00

HEATER #1 - Δ

Fig. G.61 CLADDING TEMPERATURES - HEATER NO. 2 OFF

DP 33E
1.0 KW
269 RPM
(HTR. #2 OFF)

HTR #1 - Δ
HTR #4 - Δ
HTR #2 - $\square$ #5 - Δ #7 - X

Fig. G.62 CLADDING TEMPERATURES - HEATER NO. 2 OFF

DP 214 E
 10 KW
 26 GPM
 300 ° F
 (HEATER #2 OFF)
 P/D = 1.05

Fig. G.63 CLADDING TEMPERATURES - HEATER NO. 2 OFF

Q.P. 214 E
 1.0 RUN
 26 APR 1971
 600 °F
 (HTR #2 OFF)
 PID = 1.05

Δ - HEATER #1
 ○ - HEATER #2
 □ - HEATER #3
 ○ - HEATER #4
 X - HEATER #7

— DEGREES

Figs. G.15 and G.16 with all test rods operating at 1.0 Kw, it is seen that only T/C numbers 1 and 2 (see Fig. D.11) are affected. Thermocouple numbers 1 and 2 are adjacent to coolant channels which surround H-2 and thereby record lower temperatures. For the $P/D = 1.05$ test configuration, similar results were found by comparing Figs. G.62 and G.63 to the results of Figs. G.36 and G.37. For the inner seven test rods, these results indicate that there is negligible conduction through the heater elements and coolant channels. In addition to the results shown in Figs. G.54 through G.63, asymmetric heating tests were performed at other power levels and flow rates; however, the same conclusions were arrived at.

To determine the extent of any inter-channel coolant mixing, the individual outlet coolant channel T/Cs were recorded for various asymmetric heating conditions. For the $P/D = 1.00$ and 1.05 test configurations, results are shown in Figs. G.64 and G.65 for the coolant temperature rise for individual channels. Experimental results are shown with all 19 test rods at 1.7 Kw; experimental and theoretical results are also shown with heater number H-2 at zero power. For H-2 at zero power, the theoretical results were determined by using the experimental values with all heaters at power along with the assumptions of no inter-channel coolant mixing and a $2/3$ heat input to channels adjacent to H-2. By comparing the theoretical and experimental results with H-2 off, it is evident from the $P/D = 1.00$ results, Fig. G.64, that there is essentially no inter-channel coolant mixing. Since all rods are supposedly in contact at $P/D = 1.00$, this would be expected. However, for the $P/D = 1.05$ results shown in Fig. G.65, there appears to be some inter-channel coolant mixing. This can be seen by noting that the coolant temperature rise for the two channels adjacent to the H-2 was about 8% more than

Fig. G.64 ASYMMETRIC HEATING TESTS - P/D=1.00

1.70 KW
26 gpm

ALL HEATERS ON (ACTUAL MEAS.)
HEATER # 2 - OFF (ACTUAL MEAS.)
HEATER # 2 - OFF (THEO. - NO MIXING)

Fig. G.65-ASYMMETRIC HEATING TESTS - P/D=1.05

1.70 KW
26 gpm

ALL HEATERS ON (ACTUAL MEAS.)
HEATER # 2 - OFF (ACTUAL MEAS.)
HEATER # 2 - OFF (THEO. - NO MIXING)

predicted by assuming no inter-channel mixing. For the two channels not adjacent to H-2, the coolant temperature rise was approximately 2% lower than the predicted value. While these results are not striking, they do indicate that some inter-channel mixing from the hot channels to the cold channels adjacent to H-2 is taking place. For the $P/D = 1.05$ configuration, the results are a little more striking when an examination is made of the cross coolant temperature differences. The results of Fig. G.65 show that a 20% reduction in cross coolant temperature differences can be attributed to inter-channel coolant mixing. Therefore, while the axial coolant temperature rise is affected only moderately, the cross coolant temperature differences are influenced to a greater extent by inter-channel coolant mixing. It is believed that the presence of the 30-mil spacer rings and their staggered arrangement, as shown in Fig. D.8, are responsible for the inter-channel coolant mixing that resulted for the $P/D = 1.05$ test configuration.

H. CONCLUSIONS AND DISCUSSION OF RESULTS

Heat transfer tests have been conducted for the case of turbulent longitudinal flow of NaK through closely packed rod bundles. Two triangular rod arrangements, corresponding to P/D ratios of 1.00 and 1.05, were examined. Based upon the results of the P/D = 1.00 test configuration, the following important conclusions are drawn.

- 1) In the flow range investigated, the circumferential temperature gradients and rod-average film coefficients (or Nu) are independent of the Peclet number.
- 2) Comparisons of the theoretical and experimental results have produced excellent agreement for the cladding circumferential temperature gradient and the rod-average heat transfer coefficient.

From the P/D = 1.05 test results, the following main conclusions can be made:

- 1) The presence of the spacer rings caused a significant increase in the intra-channel turbulence which in turn resulted in negligible azimuthal temperature gradients.
- 2) The heat transfer results showed a slight dependence on the Peclet number. This is probably due to a Re dependence of the added turbulence caused by the spacers.
- 3) Comparisons of the theoretical and experimental results showed poor agreement for the cladding circumferential temperature gradients and good agreement for the rod-average cladding temperature and film coefficient. This suggests that the increased turbulence intensity, which is caused by the spacer rings, has its greatest component in the azimuthal direction.

- 4) The experimental results confirmed the approximate 5-fold increase in the rod-average Nu (see Fig. B.6) in going from a contacting rod arrangement ($P/D = 1.00$) to a configuration which has all rods positively spaced at 30-mils ($P/D = 1.05$).

The test results presented earlier are strictly true only for the conditions of the experimental study. The results were determined for particular values of the following parameters: P/D , Pe , k_F/k_W , k_W/k_F , and $(R - r_3)/R$. Care must be exercised in applying the present test results to conditions other than those examined. For $P/D = 1.00$ and $Pe < 250$, excellent agreement was obtained between the theoretical predictions and the experimental results; therefore, Ref. 1 can be used with confidence in determining heat transfer results for other values of k_F/k_W , k_W/k_F , and $(R - r_3)/R$. Since the $P/D = 1.05$ test results were influenced by the presence of spacer rings, the theoretical results for $P/D = 1.05$ are more valid for rod geometries which do not employ spacer devices. For $P/D = 1.05$ and $Pe < 250$, the theoretical model can be used to determine heat transfer results for parameters - k_F/k_W , k_W/k_F , $(R - r_3)/R$ - other than those of the experimental study; however, calculations over a wide range of the above parameters, similar to Ref. 1, have not been reported in the literature.

The liquid metal heat transfer study presented here represents the first time that reliable experimental data has been obtained for closely spaced rod bundles in the low Peclet number regime. Because of "unclean" liquid metal systems, most prior rod bundle heat transfer experiments, including those at high P/D ratios, have produced anomalous results. For the present investigation, the extreme care that was taken in the experimental loop and test section design and operating procedures paid off by producing dependable results which confirmed earlier theoretical predictions.

I. NOMENCLATURE

C_p	=	specific heat of coolant, Btu/lb _m -°F
D	=	rod diameter, ft
D_h	=	$D \left[\frac{2\sqrt{3}}{\pi} (P/D)^2 - 1 \right]$ = hydraulic diameter of rod bundle, ft.
g_o	=	gravitational constant, lb _m -ft/lb _f -sec ²
h	=	heat transfer coefficient, Btu/hr-ft ² -°F
H_{fg}	=	latent heat of evaporation, Btu/lb _m
k	=	thermal conductivity, Btu/hr-ft-°F
L_H	=	heated length, ft
Nu	=	$\bar{h} D_h / k_f$ = Nusselt number, dimensionless
P	=	pitch, or distance between rod centers, ft
Pe	=	$(Re)(Pr)$ = Peclet number, dimensionless
Pr	=	$\mu C_p / k_f$ = Prandtl number, dimensionless
q''	=	heat flux, Btu/hr-ft ²
q'''	=	volumetric heat generation, Btu/hr-ft ³
R	=	rod radius, ft
Re	=	$\rho \bar{u} D_h / \mu$ = Reynolds number, dimensionless
r	=	radial coordinate, ft
T	=	temperature, °F
u	=	coolant axial velocity, ft/hr
u^*	=	$\sqrt{g_o T_w^{(e)} / \rho}$ = friction velocity, ft/hr
u^+	=	u / u^* = dimensionless velocity
W_r	=	electrical power per rod, Kw

- y = perpendicular distance from cladding outside wall (see Fig. B.1), ft
- y_o = perpendicular distance from cladding outside wall to symmetry line (see Fig. B.1), ft
- y^* = $y u^*$ = dimensionless distance
- z = axial coordinate, ft

Greek Letters

- ϵ_H = eddy diffusivity of heat transfer, ft^2/hr
- ϵ_M = eddy diffusivity of momentum transfer, ft^2/hr
- θ = azimuthal coordinate, radians
- μ = viscosity of coolant, $\text{lb}_m/\text{ft-hr}$
- ν = kinematic viscosity of coolant, ft^2/hr
- ρ = density of coolant, lb_m/ft^3
- σ = surface tension, lb_f/ft
- τ = shear stress, lb_f/ft^2
- ψ = ϵ_H/ϵ_M , dimensionless

Subscripts

- b = bulk coolant
- F = fuel or MgO region of test rod
- f = coolant
- H = heated region of test rod
- i = inside
- l = liquid water
- o = outside
- r = radial component
- v = water vapor

w = cladding wall
z = local axial value
1 = region -1 (see Fig. B.1)
2 = region-2 (see Fig. B.1)
3 = region-3 (see Fig. B.1)
 ∞ = fully developed value
 θ = azimuthal component
Other
 $(\bar{\quad})$ = average value
' = calculated quantity

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