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BUOYANCY EFFECTS ON SODIUM COOLANT TEMPERATURE PROFILES
MEASURED IN AN ELECTRICALLY HEATED MOCK-UP OF A 61-ROD
BREEDER REACTOR BLANKET ASSEMBLY*

MASTER

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ABSTRACT

This paper describes test results selected to demonstrate the effect of buoyancy on the temperature profiles in a 61-rod electrically heated mock-up of an LMFBR radial blanket assembly. In these assemblies, heat transfer occurs over a wide range of complex operating conditions. The range and complexity of conditions are the result of the steep flux and power gradients which are an inherent feature of the blanket region and the power generation level in an assembly which can vary from 20 to 1100 kW.

To provide effective cooling of all assemblies and economical operation, coolant is metered to groups of assemblies in proportion to their ultimate power level. As a result, the assembly flow can be in the laminar, transition or turbulent range. Because of the steep power and resulting temperature gradients and range of coolant flow velocities, heat transfer from rods to coolant may take place in the forced, natural or mixed convection mode. Under these conditions buoyancy may affect the flow pattern and thus alter the temperature distribution. The complexities are further compounded since, in addition to temperature gradients within an assembly, there are also significant temperature differences between adjacent assemblies. This results in heat transfer by conduction between adjacent assemblies, which tends to further distort flow and temperature patterns.

Since these effects cannot be confidently predicted analytically, full size radial blanket assembly heat transfer tests are being conducted using electrically heated fuel rod simulators in flowing sodium. A 61-rod electrically

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heated radial blanket assembly mockup of prototypic dimensions was designed, constructed, and installed in a 200 gpm ($45 \text{ m}^3/\text{hr}$) sodium test loop.

Heat transfer tests are being conducted over a wide range of sodium flow rates with this full-scale, vertical, electrical resistance heated rod bundle. The rod bundle is extensively instrumented by thermocouples located at six distinct elevations in the wire wrap and inside the heater cladding. Tests were conducted covering the flow range from fully turbulent to fully laminar with approximately constant power-to-flow ratio and row-to-row power input gradients (of ~ 3 to 1 maximum to minimum) for all tests. The temperatures were measured and resulting profiles at all six elevations compared as a function of Reynolds Number. Plots show the profiles obtained with turbulent flow ($Re \sim 7000$), through fully laminar flow ($Re \sim 500$). As predicted by theory⁽¹⁾, the temperature distribution in laminar flow was quite flat even for the steep transverse power input gradient. This reflects the buoyancy-induced flow increase in the more highly heated region of the rod bundle.

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1.0 INTRODUCTION

The principal purpose of the blanket of a breeder reactor is to increase the conversion of fertile to fissile material by utilizing neutron leakage from the core. For efficient breeding, a high volume fraction of the fertile material is required. In the LMFBR, this is achieved by a design consisting of cylindrical stainless steel tubes which contain fertile pellets, and are arranged in tight bundles of triangular pitch contained in a hexagonal duct. To minimize hot spots and permit adequate cooling, the rods are separated by spacer wires wound helically around each rod. Coolant flow must be maintained over a wide range of operating conditions which include the entire spectrum of modes of flow: turbulent, transition and laminar. For a safe design, the temperature distribution in the coolant must be known at all operating points. The prediction of temperature distributions are difficult because of the complex and wide range of operating conditions which may occur in blanket assemblies. These complexities are introduced by the following features and requirements inherent to the LMFBR blanket region:

1. The steep flux gradient causes steep heat generation and temperature gradients across the blanket and the individual assemblies.
2. In order to approach uniform assembly outlet temperature, the flow to the assemblies is orificed to distribute the coolant flow in proportion to the assembly heat generation rate and since, because of assembly management schemes, it is desirable for the radial blanket assemblies to be interchangeable, a wide range of flow rates exists, covering from startup to full load, all modes of flow (laminar to turbulent).
3. Because of the heat input gradient, buoyancy contributes significantly to the flow and the resulting temperature distributions at low flow operation.
4. Heat transfer from adjacent assemblies alters the temperature distribution predicted for individual assemblies, especially at lower flows.

The temperature distribution can be predicted by subchannel codes of the marching type. However, these codes contain parameters whose values must be obtained from or checked by calibration against test data. The code parameters which must be obtained or checked experimentally include momentum interchange and conduction between flow channels, turbulent mixing and the effect of buoyancy, which becomes significant at low flows. No test data existed which were directly applicable to radial blanket assemblies, and which could be used to describe the effect of buoyancy on flow and temperature distribution in wire wrapped rod bundles. Since these effects cannot be confidently predicted analytically; full size radial blanket assembly heat transfer tests are being performed using electrically heated fuel rod simulators in flowing sodium. The purpose of this paper is to present experimental data to demonstrate the effect of buoyancy on the temperature distribution in a 61-rod electrically heated model of an LMFBR blanket assembly.

2.0 TEST SECTION DESIGN

The heat transfer test section was required to be an accurate dimensional mock-up of a radial blanket assembly. Additionally, the test section was required to reproduce as closely as practical the inlet flow conditions, power level and power distribution of blanket fuel rods and assemblies at various stages of its life in a reactor. A comparison of the major characteristics of a typical radial blanket reference design with the design objectives for the 61-rod test section is shown in Table 1.

The test section consists of: 1) an instrumented rod bundle containing 61 wire wrapped electrically heated fuel rod simulators; 2) a hexagonal duct which provides the flow boundary around the rod bundle; 3) two auxiliary ducts containing five simulator rods each mounted adjacent to opposite faces of the duct for test of cross-duct heat transfer effects; and 4) a 10-inch by 11-foot long sodium containment vessel.

The test section is oriented vertically. As shown in Figure 1, sodium enters at the bottom and flows upward through the heater rod bundle, dis-

charging from the duct near the vessel exit nozzle. The duct is connected to the sodium inlet line by a bellows, which provides a positive seal to prevent sodium from bypassing the duct and accommodates differential expansion between the duct and vessel. The bottom ends of the heater rods are free to expand axially downward. The space outside the duct contains stagnant sodium. It is compartmentalized by numerous horizontal and radial baffles to minimize heat loss by convective transfer.

Figure 2 presents a horizontal section through the test assembly showing the sixty-one rods contained in the hexagonal duct. The rods are spaced by helically wound wires of 4-inch (10.16 cm) lead. The wires contain chromel-alumel thermocouple junctions at three of the levels of measurement shown in Figure 1. The auxiliary ducts and heaters were not used in the test series presented in this report. Each simulator rod contains a resistance coil of nichrome wire wound helically at varying lead to generate heat at an axially chopped cosine shaped rate of 1.4 maximum-to-average ratio over a 45-inch (114.3 cm) length.

The power is supplied separately to each of the nine rows of rods by saturable core transformers. Sodium is pumped through the test section from a large-volume test loop equipped with control valves, electromagnetic pump and flow meters, and an air-blast heater-cooler to maintain constant sodium supply temperature. Details of heater rod design and of wire wrap thermocouple construction and attachment were discussed in Reference 3, as were the data acquisition system, test method and calibration procedures.

3.0 INSTRUMENTATION

Since the test results were to be compared with predictions of subchannel codes of the marching type (COBRA⁽⁴⁾, COTEC⁽⁵⁾, THI-3D⁽⁶⁾, etc.), the temperature sensors were grouped into distinct horizontal planes at selected elevations as shown in Figure 1. Numerous thermocouples are located at the test section inlet and outlet, and at six discrete axial planes along the bundle assembly. These couples were located at the vessel and duct walls,

in the wire wraps and inside the heater rods, attached to the inner surface of the cladding (see Figure 2). Resistance thermometers at the inlet, outlet and the duct wall at the six elevations were used for calibration. Flow, temperature and power input measurements were recorded on tape and discs by a computerized data acquisition system. Details of the data acquisition system and the calibration procedure were given in Reference 3. Power input to each row of rods was measured by a Hall-effect power computer. The sodium flow rate was determined by an electro-magnetic flow meter.

Figure 3 shows an example of the locations of the thermocouples together with one set of normalized temperature data at one level of measurement at one point for one test run. From such maps, temperature profiles were plotted across the bundle at several elevations. The temperature data are presented as a normalized temperature rise defined as:
$$\frac{T - T_{in}}{Q/WC_p}$$

4.0 TEST PROCEDURE AND PARAMETERS

The general test procedure was to adjust the test loop operating parameters to achieve the desired sodium flow and inlet temperature, then increase bundle power gradually to the value required to produce the desired test section power gradient and temperature rise ($\sim 200^\circ\text{F}/\sim 110^\circ\text{F}$). The approach to steady state power conditions was monitored by printing out selected temperatures, flow, power and pressure drop. Criteria for steady state were constancy of power, flow, inlet temperature ($\pm 5^\circ\text{F}/2.8^\circ\text{C}$) and ΔT ($\pm 5^\circ\text{F}/2.8^\circ\text{C}$) as indicated by the absence of trend lines for at least 30 minutes. When nominal steady state conditions were achieved, a file of 550 readings of each of 524 data channels was acquired by the DAS and logged on magnetic tape over a period of approximately 10 minutes. A sample of this data consisting of the average of 20 readings on each channel was printed out and used as the basis for this paper. The twenty sample averages were selectively checked against the respective average of all 550 readings and found to be essentially equivalent. This data acquisition procedure was selected to provide a statistically valid sample, which would be quickly available, and useful for testing consistency and steady state criteria.

In this test series, a power input gradient of approximately 2.8 to 1.0 maximum-to-minimum was maintained (as shown in Figure 3). Inlet temperature was near 600°F (316°C) and mean temperature rise near 200°F (111°C). Table 2 presents the flow parameters of the four tests.

5.0 RESULTS AND DISCUSSION

Prior to the tests, pre-test predictions were made using the COBRA-IV⁽⁴⁾ and other subchannel analysis codes. These indicated that with fully turbulent flow ($Re > 5000$) the velocity was nearly uniform over all the inside sub-channels. Figure 4 shows the velocity traverses predicted by COBRA in the direction of the power input gradient at the heated zone midplane, and at two higher elevations (55 in/140 cm and 80 in/203 cm) for turbulent and laminar flow. The predicted effect of buoyancy is an acceleration of the flow at the hotter side of the rod bundle producing, at lower flows, a velocity gradient in the heated zone. This gradient is then reduced by mixing in the unheated region above the heated portion of the rod bundle. An earlier analysis⁽²⁾ showed that this flow redistribution causes a flattening of the temperature gradient. Without the buoyancy effect, i.e., at high Reynolds numbers, the temperature pattern approximates the heat input gradient (modified by inter-channel mixing and bundle peripheral circulation).

Figures 5 and 6 compare experimental data with predicted temperature profiles for turbulent and transition flow through the bundle. The measured data suggest that the mixing factors employed (see Table 3) in the COBRA code under-predicted, and in the COTEC code over-predicted the peak value and the steepness of the gradient obtained experimentally at heated section outlet and above. When the flow is reduced while maintaining the same power-to-flow ratio and 2.8:1 power gradient, the effect of the buoyancy-induced acceleration at the hotter side of the assembly becomes evident (Figure 7), since the temperature gradient is significantly flattened. At a Reynolds number of 490 (Figure 8), the temperature gradient nearly disappears even though the power input gradient remains as steep as in the previous tests.

The predicted temperature profiles in Figure 7 obtained by the COBRA code neglect the effect of conduction. Conduction should also flatten the temperature profile but its effect is expected to be smaller than buoyancy. If conduction is included COBRA should provide a better fit with the test results.

6.0 COMPARISON WITH INFORMATION IN THE LITERATURE

Test data for buoyancy effects in liquid metal flowing axially through vertical rod bundles are scarce. The theoretical analysis of Ramm and Johannsen⁽⁸⁾ predicts velocity peaking due to buoyancy which is higher than that shown by the COBRA analysis for a rod bundle of similar dimensions and power skew. However, a Rayleigh number of 5 was used while Ra was ~1.0 in the test case (the higher Rayleigh number represents stronger buoyancy). A flatter velocity distribution was also predicted analytically for a narrow rectangular geometry in Reference 2. Buhr, et al⁽⁹⁾ developed a buoyancy effectiveness criterion:

$$Z = Ra/Re \times D/L$$

For buoyancy to affect the velocity distribution, Z has to be greater than .002, as was the case for the tests at Reynolds number of 990 and 490 (Table 2). Flattening presumably due to buoyancy was definitely observed at these Reynolds numbers and may be operative even at Re = 3700.

7.0 CONCLUSIONS

The effect of buoyancy on the flow distribution in a complicated geometry was demonstrated experimentally by measuring the temperature distribution over a range of flows including turbulent and laminar modes of flow. In a companion paper⁽⁷⁾ to be published, it was demonstrated that for the test geometry, the transition from purely turbulent to purely laminar flow took place in a stable, continuous manner over the Reynolds number range of 5000 to 500. Further, the buoyancy force more than compensates for the increase of friction factor at low flow to redistribute the flow in a rod bundle with a steep heat input gradient. This tends to flatten the temperature profile.

At the higher laminar flow, the test results could be predicted with reasonable accuracy by a marching type subchannel code (COBRA-IV). Purely analytical methods^(2,8) predicted the nature of the change in temperature and flow distribution induced by buoyancy, but not the exact magnitude for a number of reasons, which include differences in geometry and simplifying assumptions to aid in the solution of the problem.

8.0 NOMENCLATIVE

D = rod diameter

De = rod bundle averaged hydraulic diameter

g = acceleration of gravity

Gr = Grashof No. based on film drop = $g\beta\Delta TDe^3/\nu^2$

Gr* = Grashof No. based on axial temp. gradient = $g\beta \frac{\Delta T}{\Delta L} \frac{De^4}{\nu^2}$

P = triangular rod pitch

Pr = Prandtl No.

Ra = Reyleigh No. = Gr x Pr

Re = Reynolds No. based on bundle average velocity and De

Z = Buoyancy criterion (reference 9)

β = compressibility

ν = kinematic viscosity

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TABLE 1

TEST SECTION DESIGN

<u>PARAMETER</u>	<u>RADIAL BLANKET ASSEMBLY</u>	<u>TEST SECTION</u>
Number of Rods	61	61
Rod Diameter, in/cm	0.520/1.32	0.519/1.32
Length of Fuel Stack or Heated Zone, in/cm	64/163	45/114.3
Total Length, in/cm	~105/~267	104.4/265
Wire Rod Spacer Diameter, Internal Rods, in/cm	0.037/.094	0.037/.094
Wire Wrap Pitch, in/cm	4/10.16	4/10.16
Triangular Rod Pitch, in/cm	0.560/1.42	0.5615/1.43
Pitch-to-Diameter Ratio	1.077	1.082
Duct ID, in/cm	4.45/11.3	4.49/11.4
Axial Power Distribution, Cosine, Maximum/Average	1.3-1.6	1.40
Peak Linear Power, kW/ft-kW/m	7-16/23-53	.4-14.8/13-48
Maximum Rod Power, kW	36	40
Maximum Assembly Power, kW	1100	900
Radial Power Distribution, max/avg.	1.9-2.2	2.0
Sodium Inlet Temperature, °F/°C	500-800/260-425	500-800/260-425
Sodium Exit Temperature, °F max/°C max	800-1000/425-540	1050/565
Sodium Flow Rate, gpm, m ³ /hr.	40-150/9-34	5-150/1.14-34
Sodium Flow Velocity, ft/sec - m/sec	1.6-8/.5-2.5	0.2-8/.06-2.5

TABLE 2

TEST PARAMETERS

Reynolds No. (Based on Bundle Flow and Hydraulic Diameter)	7,300	3,700	990	490
Grashof No. (Based on Film Temp. Drop)	484,000	426,000	213,000	96,800
Ratio, Gr/Re^2	.0091	.031	.218	.403
Flow Rate, gpm	34.1	16.8	4.6	2.3
Flow Rate, $m^3/Hr.$	7.7	3.8	1.045	.522
Average Velocity, ft/sec.	2.02	1.03	.274	.135
Average Velocity, m/sec.	.615	.313	.083	.041
Grashof No. (Based on Axial Temp. Gradient)	35,000	35,000	35,000	35,000
Buoyancy Parameter, Z (see Section 6.0)	.0004	.0008	.003	.006
Heat Input, kW	265	132	33.7	17

Buoyancy effective when $Z > .002$ (Reference 9)

TABLE 3

EMPIRICAL FACTORS EMPLOYED IN THE CODE PREDICTIONS

1. COBRA-IV

Ratio of turbulent interchange to axial flow: $\beta = .01$

Transverse momentum parameter: $SL = 0.208$

2. COTEC

Ratio of turbulent interchange to axial flow: $\beta = .015$

Fraction of flow following the wire wrap: $\beta = 1.00$

Fraction of area under wire wrap causing internal sweeping: 1.0

Fraction of area under wire wrap causing peripheral sweeping: 1.50

Figure 1. Diagram of Thermocouple Locations in 61-Rod Radial Blanket Test Assembly – Elevation View.

Figure 2. Conceptual Test Section Cross Section

Figure 3. Typical Temperature Distribution at Elevation A (55"/140 cm) Normalized

Figure 4. COBRA-IV Prediction of Cross-Assembly Velocity Distribution for Laminar ($RE = 990$) and Turbulent ($RE = 7300$) Flow

Figure 5. Temperature Traverses Across Test Assembly - Reynolds No. = 7300

Figure 6. Temperature Traverses Across Test Assembly -- Reynolds No. = 3700

Figure 7. Temperature Traverses Across Test Assembly - Reynolds No. = 990

Figure 8. Temperature Traverses Across Test Assembly – Reynolds No. = 490