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FLOW CHARACTERISTICS OF THE
EBR-II CORE-TYPE SUBASSEMBLY

by

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NOMENCLATURE

<u>Symbol</u>	<u>Description</u>	<u>Units</u>	<u>Symbol</u>	<u>Description</u>	<u>Units</u>
a	Constant	Arbitrary	x	Axial distance along fuel	ft
A	Area	ft ²	y	Transverse distance along traverse path	in.
b	Distance through light-attenuating fluid	ft	ρ	Density	lb _M /ft ³
c	Specific heat at constant pressure	B/lb _M -°F	μ	Viscosity	lb _M /ft-sec
C	Dye concentration	Arbitrary	<u>Subscripts</u>		
C'	Heat conductance per unit length of channel	B/ft-°F-sec	c	Coolant	
d	Dimension	ft	eff	Effective	
D	Diameter	ft	f	Fuel	
g _C	Conversion factor	lb _M -ft/lb _F -sec ²	FE	Fuel element	
I	Light intensity	Arbitrary	i	Individual	
k	Thermal conductivity	B/ft ² sec-°F	i	Incident upon transparency	
K	Mixing coefficient	ft ⁻¹	ℓ	Leaving transparency	
ℓ	Distance	ft	m	Model	
L	Total length	ft	max	Maximum	
$\dot{m}$	Mass flowrate along channel	lb _M /sec	n	Individual channel	
$\dot{m}'$	Effective mass flowrate between channels, per unit length of channel	lb _M /sec-ft	N	Individual hexagonal-row number	
n	Length ratio	Dimensionless	Na	Sodium	
N	Number	Dimensionless	O	Source	
p	Pressure	lb _F /ft ²	p	Prototype	
$\bar{P}$	Perimeter between channels	ft	r	At radial distance r	
r	Radial or transverse distance	ft	R	Row	
r	Roughness	Dimensionless	Re	Reynolds	
q'	Heat generation per unit length of channel	B/sec-ft	S	Static	
S	Reflector savings	ft	SST	Stainless steel	
T	Temperature	°F	T	Total	
v	Velocity	ft/sec	U	Uranium	
V	Volume fraction	Dimensionless	W	Wire	
w	Weighting factor	Dimensionless			

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ABSTRACT

To assist in the determination of the relationship between power level and fuel temperatures in the Experimental Breeder Reactor II (EBR-II) fuel subassemblies, local coolant velocity measurements were made in a model subassembly almost three times the linear size of the prototype. These measurements indicate that the fuel-element spiral-spacer wires exert considerable influence on the velocities. They also indicate that no fuel element has associated with it an average coolant velocity more than 2% lower than the average velocity in the subassembly. However, since the outer halves of the exterior fuel elements have associated with them comparatively high flow areas and velocities, the coolant flowrates for most of the interior elements are as much as 18% lower than the overall average coolant flowrate per element.

Static-pressure measurements were also made on the model subassembly, to provide information concerning (a) the pressure drop across the fuel-element bundle alone, and (b) the effect of spiral spacer wires on pressure drop.

By using a photograph that illustrates the dispersion of injected dye as it passes through the fuel-element bundle, we estimated the effects of coolant mixing and of transverse heat transfer on core coolant temperatures. According to this estimate, many of the interior fuel elements receive very little cooling benefit from the relatively large coolant flowrates associated with the exterior fuel elements.

I. INTRODUCTION

A. Background Material

The Argonne Experimental Breeder Reactor II (EBR-II) was developed as one part of the U.S. Atomic Energy Commission's Experimental Power Reactor Program.^{1,2} The EBR-II is an integral nuclear power plant; it includes a complete fuel-processing and fabrication facility in addition to the reactor, the heat-transfer system, and the steam-electric plant. The

thermal power rating of the reactor is 62,500 kW; gross electrical power output is 20,000 kW. Construction started in early 1958, dry criticality occurred in September 1961, and power operation of the reactor began in 1964.

The reactor is of the heterogeneous type, unmoderated, and sodium-cooled. At the reactor inlet, the sodium temperature is 700°F, and at the outlet, about 884°F. The reactor core is shaped approximately like a right circular cylinder, with an L/D ratio of about 0.8. Maximum core power density is 1230 kW/liter of core volume, or 3870 kW/liter of fuel-alloy volume. Breeding blankets of depleted uranium surround the core on top, bottom, and sides. Both fuel and depleted uranium, in the form of cylindrical rods of varying diameters, are installed in subassembly tubes of hexagonal cross section, which are loaded vertically into the reactor vessel.

The core-type subassembly (shown in Fig. 1) consists of three "active" sections: lower blanket, core, and upper blanket. The core section consists of 91 "pin-type" fuel elements (as shown in Fig. 2), each consisting of a right circular cylinder (pin) of fuel alloy (0.144-in. OD, 14.22 in. long) fitted into a thin-walled stainless steel tube (0.174-in. OD, 0.009-in. wall). The 0.006-in. annulus between the pin and the inside of the tube is filled with stagnant sodium for a thermal bond; the coolant sodium flows along the outside of the tube. The tube is somewhat longer than the pin, to provide room for excess bonding sodium and for an inert-gas space to accommodate expansion of the bonding sodium. The fuel elements are contained within the hexagonal subassembly tube, being fastened in the subassembly at their lower ends by slotted tips hooked to a grid of parallel bars. A single, helical wire (0.049-in. OD, 6-in. pitch), welded to the outside of each fuel element, maintains the spacing of the elements in a triangular lattice.

The upper and lower blanket sections are identical in construction. Each consists of 18 pin-type blanket elements (shown in Fig. 2), also spaced in a triangular lattice. The unalloyed depleted uranium pins (0.316-in. OD, 18 in. long) are again fitted into a stainless steel tube (0.376-in. OD, 0.022-in. wall), the 0.008-in. annulus being filled with bonding sodium. The blanket elements are positioned transversely in the subassembly tube by two grids, one at each end of the elements, so that no spacing provisions are made along the length of the elements. A central steel rod, which fits into the triangular lattice and has the same OD as the elements, ties the two grids together.

The lower adapter of each subassembly engages the reactor grid and contains orificing holes through which the coolant enters the subassembly from the high-pressure, inlet-coolant plenum chamber. The coolant leaves the subassembly at the upper adapter, which includes a knob for use by the fuel-handling and -transfer equipment.

112-572 Rev. 2

Fig. 1. EBR-II Core-type Subassembly (all dimensions in inches)

112-2790 Rev. 1

Fig. 2. Core Subassembly Elements (all dimensions in inches)

B. Justification for This Study

The work described in this report was required because of the importance of knowing the core fuel-element temperatures. For example, excessive thermal cycling and burnup damage to the fuel pins will occur if the temperature of the fuel alloy (uranium-5 w/o fissium) exceeds 1200°F. No instrumentation is installed in the core-type subassembly; the fuel-element temperatures can be determined only by making calculations based on measurements of the (a) reactor high-pressure- (core and inner blanket) and low-pressure- (outer blanket) system coolant flowrates, (b) reactor-coolant inlet temperature, (c) average reactor-coolant outlet temperature, and (d) coolant temperatures near some of the subassembly upper adapters in the reactor outlet plenum. Making these calculations requires the use of the coolant flowrate associated with each fuel element. This flowrate depends on the coolant velocities near that element. However, because of the complicated geometry of the core section of the core-type subassembly, local velocities cannot be ascertained by analytical techniques. For these related reasons, the velocity distributions in the subassembly had to be measured; this report describes the equipment and techniques used in making the measurements, and presents the results obtained. Some local pressure-drop measurements are also described. Appendix B presents an estimate of the effects of coolant mixing and of transverse heat transfer on coolant temperatures within the core-type subassembly.

II. APPARATUS

A. Probes

During the early preparations for measuring EBR-II core velocities, it was decided (for reasons given in Section V below) that the best primary sensing device for measuring those velocities is the pitot-static probe, which transmits local total (also called impact or stagnation) and static pressures simultaneously and makes it possible to calculate local velocities using the expression³

$$v = \left[\frac{2g_c(p_T - p_S)}{\rho} \right]^{1/2} \quad (1)$$

The actual probes used, supplied by the Aero Research Instrument Co., Inc., under catalog No. HS-1018-M, have an OD of 0.042 in. Trying to find or develop smaller probes was considered inadvisable since the 0.042-in.-OD probe holes are only about 0.010 in. in diameter, promising more than ample plugging and response problems.

B. Model Subassembly

Since the prototype subassembly has only 0.019-in. gaps available for probe traverses, use of 0.042-in.-OD probes meant that we had to use a model subassembly more than twice as large linearly as the prototype. This requirement was not dismaying, however, because the flow passages in the prototype are so small that velocity measurements made in a prototype would have poor definition and be influenced adversely by the continuous proximity of the fuel elements and probe.

In fact, a model five or six times the linear size of the prototype would have been highly desirable, but circumstances did not permit this. Since the model was to be larger than the prototype, and since water was to be used in the model instead of sodium (the EBR-II coolant), principles of similitudes (dimensional analysis) had to be obeyed if the water-model measurements were to be used for predicting the flow characteristics of the sodium prototype. Therefore, as shown in Appendix A, the Reynolds Number (N_{Re}) in the model had to be the same as that in the prototype. Because of capacity limitations of the available water pump, this criterion could not be satisfied unless the model were no more than three times the linear size of the prototype. The ratio actually used was 2.874, which permitted assigning the convenient rod OD of 0.50 in. to the most abundant component of the model, the fuel element.

Table I gives a general comparison of the prototype and the model and indicates that the model blanket elements are only about half as long as they should be to constitute a perfect model. This discrepancy, which

made possible a substantial reduction in the overall length of the model subassembly, is permissible because the only purpose of these elements is to simulate entrance and exit conditions for the core section. Since the L/D of the elements is still over 33, they are long enough for this purpose.

TABLE I. Comparison of Prototype and Model

	Prototype	Model
Fuel-element OD, in.	0.174	0.500
Fuel-element length, in.	18	52
Pitch of fuel-element spacer wires	6.0	17.2
Blanket-element length, in.	21	29
Average core-coolant velocity, ft/sec	26.1	13.8
Average velocity in gap at core exit, ft/sec	11.7	6.2
Reynolds number, core	72,100	81,100
Elevation of lower traverse (see Figs. 9 and 11 later), in.	5.72	16.44
Elevation of upper traverse (see Figs. 10 and 12 later), in.	15.46	44.43

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Fig. 3. Water Loop with Model Subassembly Installed

Figure 3 shows the exterior of the hexagonal tube of the model subassembly. The tube is made of two stainless steel halves, which were tack-welded together, the longitudinal seams then being sealed with soft solder. This method of fabrication reduced the possibility of warpage during the fabrication. Figure 4 shows the assembly of the lower blanket, the fuel-element grid, the fuel-element bundle, and the upper blanket assembly, all of which are brass and fit into the hexagonal tube. These assemblies are held in place in the tube with screws. Figure 5 pictures the assembled probe supports, which are welded to the hexagonal tube, from several different aspects, and Fig. 6 shows the disassembled components of a probe support. The design of the probe supports was copied almost exactly from that used by APDA,⁴ the only changes being related to the problem of assembling the support components around a probe with the tip already bent.

112-109

Fig. 4. Assembly of Lower Blanket, Fuel-element Grid, and Fuel-element Bundle (below) and Upper Blanket Assembly

Fig. 5

Assembled Probe Supports

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The inside wall of the prototype subassembly hexagonal tube has numerous ridges, which are made by forming grooves in the outside of the wall. These ridges help maintain rigidity of the fuel-element bundle and reduce leakage of coolant along the wall. In the model, six of the ridges are inserted by using plugs sealed with soft solder, so that the ridge heights may be altered simply by exchanging plugs; the remainder of the ridges are bolted to the wall of the hexagonal tube.

Figure 7 shows one of the pitot-static probes together with several accessories. The sealing-disk assembly is slipped around the bent tip of the probe, and then the flag is soldered to the probe at the bend. In operation, the flag slides in the guide, which is held by end pieces of the probe support. The guide keeps the probe from being bent or broken by the flowing water and ensures that the probe tip points directly upstream.

C. Loop

The model subassembly was installed for testing in the loop shown in Fig. 3. Starting with the pump

112-110

Fig. 6. Components of Probe Supports

at the lower rear, water flows to the right through a clamped expansion joint and then to the front through a standard ASME orifice section. The water then passes to the left through a throttle valve, enters an 8-in.-diam tee, rises through the model subassembly, enters another 8-in.-diam tee, drops through a second throttling valve, and then returns horizontally to the pump suction through a second clamped expansion joint. (The expansion joints were intended to isolate pump vibrations from the loop, but loop vibration was found to be less with the joints clamped.) Most of the piping is 4 in. in diameter except for a 6-in.-diam pump-suction section. The second throttling valve (in the vertical line) is bypassed with a 3-in.-diam line containing a valve and a filter.

111-9481

Fig. 7. Pitot-static Probe, with Flag, Sealing-disk Assembly, and Guide

The entire loop is copper, brass, or bronze, except for the cast iron pump case and 8-in.-diam blind flanges; the stainless steel expansion joints, head tank, filter, orifice plates, and model subassembly components; the rubber gaskets; and the mild steel bolts, washers, and nuts. During several months of operation, no problems associated with electrochemical action on the dissimilar metals occurred. The pump casing and blind flanges were copper-plated, but rusting occurred nevertheless, presumably because of a poor plating job.

Loop pressure is maintained by the head tank shown at the upper right of Fig. 3, the tank being connected to the loop near the pump suction.

Another test section, which fits into this loop, is used for determining the relationships between pressure drop and flowrate for the various EBR-II subassemblies, including control rods.

The pump, driven by a 50-hp motor, delivers about 730 gpm at a head of 220 ft.

D. Differential-pressure Measuring Devices

Since manometers are straightforward and accurate, it would have been advantageous to use them for measuring differential pressures produced by the probes; indeed, several manometers were made available for that purpose. However, it was suspected (and later found to be true) that displacing the manometer fluid with fluid passed through the small probe holes would require unreasonable amounts of time to collect data. Therefore, differential-pressure transmitters, which require relatively small amounts of fluid flow to produce readings, were also made available. Supplied by Taylor Instrument Co. under catalog No. 200 TD 111, they have fixed ranges of 0 to 50 in. of water. Their output was read on Taylor receiver gauges (catalog No. 44S289), which may be seen on the panel in Fig. 3. Calibrated accuracy of the transmitters is stated to be within 1% of range and, of the gauges, within $1/2\%$ of range. Figure 8 shows the manner in which the probes and transmitters were connected.

E. Water Supply

Three different types of water were used at various times in the loop: deionized, tap water softened to 85-ppm hardness, and tap water softened to almost zero hardness. Operation with deionized water from available equipment was abandoned because the loop water temperature was controlled by continuously adding cold water and draining hot water (heated by fluid friction). Water consumption was so high (1 gpm) that the deionizing equipment required regeneration at too frequent intervals. The harder tap water was used while waiting for installing of additional softening equipment. No effect of water type either on ease of operation or on results was observed.

III. PROCEDURE

A. Preparatory Routine

The loop was filled with cold water, air being vented from several high-point vent lines provided. Probe and transmitter lines were filled with water under tap pressure in the following manner (see Fig. 8):

Fig. 8. Schematic Diagram of Probe and Transmitter Piping

Tap-water valves were opened, and water was allowed to flow vigorously through the vent-line valves at the tops of the transmitters, to ensure that no air was left in the transmitters. These vent-line valves were then closed, and couplings provided near the probes were opened, water being allowed to flow vigorously through the lines between the transmitters and the probes. Short lines between the probes and the couplings were filled from a syringe; then the couplings were reconnected,

with considerable water pressure still on them. Tap-water pressure was then left on the system for several minutes, with apparent success at driving any air in the probes themselves into the loop. The foregoing procedures were followed, to try to eliminate differential-pressure reading errors caused by air in the instrument lines; the procedures evidently were satisfactory because the data were reproducible over an extended period of time, using different probes, transmitters, and lines.

The pump was then started, and the desired flowrate was obtained by adjusting the throttle valve at the inlet to the model subassembly. The throttle and bypass valves downstream from the model subassembly were always left wide open; with this method of operation, the pressure on the model was as low as it could be, and chances of blowing out the soft-solder joints were reduced. (With other test sections, the inlet throttle valve has been used to get the desired test-section inlet pressure, the downstream throttle valve being used to control flowrate.)

After the pump was started, it took about one-half hour for the loop to reach the normal operating temperature of 160°F, maintained (as mentioned before) by adding cold water and draining hot water. Although higher temperatures could have been obtained readily, 160°F was used because it reduced the possibility of soft-solder joint failures, of bad burns resulting therefrom, and of occurrence of cavitation.

B. Data Gathering

Probe differential-pressure readings were then recorded, usually with four probes tended simultaneously. Data were taken at points adjacent to fuel elements and at five equally spaced intermediate points. Since the fuel-element spacing in the model, along the probe paths, is 0.321 in., this means that the readings were made about every 0.054 in. Probe-tip positions were determined by measuring the distance between the face of the probe sealing-disk assembly and a lateral scribe on the 1/4-in.-OD tube that forms the "handle" on each probe. The measuring was done with inside calipers and the decimal scale on a vernier caliper, and is believed to have been accurate within 0.015 in. This procedure, then, was equivalent to taking readings in the prototype every 0.019 in. with an accuracy of ± 0.005 in. in absolute location. The distance from the face of the probe sealing-disk assembly to the outer surface of the hexagonal tube, the thickness of the wall of the hexagonal tube, the distance from the probe tip to the probe scribe, the distances from the sealing-disk assembly face to the probe scribe for both fully withdrawn and fully inserted probes, the distance from one inside surface of the hexagonal tube to the other (along the probe paths), and the probe-tip diameter were all checked for consistency and found to agree within the above-stated limit of accuracy.

On the average, it took about 20 min to get each reading, because of the slow rate at which fluid flowed through the small probe holes to move the transmitter diaphragms. For this reason, about 1 week was required to get a complete set of readings at any one probe location. Other differential-pressure transmitters requiring less displacement of fluid are available, but they are substantially more expensive than the ones used and often harder to get on short notice. (The transmitters used can withstand Δp 's as large as the working pressure; if devices had been used that did not have this feature, they would have been damaged on many occasions.) As far as using manometers goes, about 8 hr were required to obtain each reading!

C. Plugging and Other Anomalies

Considerable difficulty was encountered with plugging of probes. It soon became standard procedure before the installation of any probe to immerse the probe tip in a beaker of water and blow air, first through the impact hole and then through the static holes, from the handle end of the probe. The number of air bubbles leaving the tip holes gave a good indication of the speed with which readings could be obtained with that probe. If few or no bubbles were observed, the tip was immersed in a beaker of nearly boiling Oakite No. 31 solution, which had a perfect record for opening up completely or partially plugged probes. Care was taken to rinse off the Oakite after the cleaning operation, since dried Oakite appeared to cause plugging itself.

Some confusion was caused by the fact that the probes and transmitters respond much faster to static-pressure changes than to impact-pressure changes, because of the comparatively high resistance to flow of the probe-impact channels as compared with that of the static channels. As a result, increases in subassembly flowrates initially produced reduced, and often negative, differential-pressure readings. These misleading transient readings gradually corrected themselves and assumed correct steady-state values in the aforementioned reading time of about 20 min. Similar behavior, on a less spectacular scale, often occurred when probes were moved from one position to another while a traverse was being made.

An ANL-made pitot-static probe (made of 0.062-in.-OD, 0.010-in.-wall and 0.032-in.-OD, 0.008-in.-wall tubing) gave almost immediate response with the transmitters. These dimensions provide channels only slightly larger than those of the Aero Research probes. It therefore appears that by using slightly thinner tubing, one could make probes that would have better response characteristics than the Aero Research probes and would be no larger or even smaller.

Repeated handling of the probes, especially with the sealing-disk assemblies hanging on the small tubing, produced cracks in most of the probes at the joint between the small tubing and the handle. These cracks were readily sealed with silver solder.

IV. RESULTS FROM VELOCITY MEASUREMENTS

Figures 9-13 present results from data obtained from probe traverses in the model EBR-II core subassembly. The plotted velocities were calculated from the receiver gauge readings, using Eq. 1. All readings were taken with a flowrate of 91.5 lb/sec through the model subassembly, as indicated by the pressure differential across the flow-measuring orifice. This flowrate produced an average velocity of 13.8 ft/sec at any transverse cross section in the model fuel-element bundle and an average velocity of 6.2 ft/sec in the open region at the core exit. This flowrate also produced, in the model fuel-element bundle, an N_{Re} of 81,100, which is somewhat higher than the 72,100 value associated with the prototype at rated power. The higher N_{Re} in the model was used because it caused higher probe differential pressures, thus reducing inaccuracies introduced by transmitter or receiver gauges. Since both model and prototype N_{Re} 's are well into the turbulent-flow regime, this difference in N_{Re} 's should not have resulted in any significant difference between the normalized velocity profiles of the model and the prototype.

Note that the word "velocity" as used in this report refers to the magnitude of the velocity vector; that is, it does not refer to the magnitude of the axial component of the velocity vector, nor of the component in the direction of the spiral wires. (This latter component is ambiguous because many locations in the subassembly are equidistant from two wires, each heading in a different direction.) However, the velocities given here can include errors from several sources. As reported by Folsom,⁵ one possible source of error is yaw, the condition under which the direction of the velocity vector is not parallel to the probe head. As indicated in Section II.B above, a major effort was made to hold each probe so that at all transverse positions the probe head was parallel to the subassembly axis. Therefore, according to Folsom's Fig. 6, if the direction of the velocity vector at the probe location were not parallel to the subassembly axis, the probe Δp indicated a velocity that was from +1% in error at 12° yaw to -3% in error at 24° yaw. Because of the difficulties involved, no serious attempt was made to measure yaw angles, but several relevant facts can be mentioned. These are: (a) the spiral wires cross the fuel-element axial surface elements at an angle of less than 7°, and (b) because of the above-mentioned different headings of adjacent wires at any axial position, it should be difficult for most of the fluid at any transverse cross section to adopt directions that would cause yaw angles even as large as 7°.

A second possible source of error in the velocity values is turbulence, which may produce probe Δp 's somewhat higher than the true mean Δp . A third source of error is the displacement, in a two-dimensional velocity field, of the effective center of the impact pressure from the probe geometric center toward the region of higher velocity. This possible displacement appears to be less than the 0.015-in. uncertainty in probe

Fig. 9. Velocity Traverse at Center Location, 16.44 in. Downstream from Ends of Fuel-element Spacer Wires

Fig. 10. Velocity Traverse at Center Location, 44.43 in. Downstream from Ends of Fuel-element Spacer Wires

Fig. 11. Velocity Traverse at Edge Location, 16.44 in. Downstream from Ends of Fuel-element Spacer Wires

Fig. 12. Velocity Traverse at Edge Location, 44.43 in. Downstream from Ends of Fuel-element Spacer Wires

Fig. 13. Velocity Traverse at Center Location, Downstream from Core Exit

locations. And, a fourth possible source of error, already mentioned in Section II.B, is the mutual-interference effect between probe and solid boundaries; this error source was taken into account somewhat while calculating flowrates, as explained below.

The results indicate that the spiral spacing wires play, as might be expected, an important role in establishing the velocity profiles. In Fig. 9, for example, the lowest velocities occur almost without exception where the probe tip makes its closest approach, about 1.2 in. downstream and 0.08 in. transversely, to each spacer wire. This same behavior is apparent in Figs. 11 and 12, but is not quite so obvious in Fig. 10. (Another explanation that has been offered for the low points is that they occur where the probe tip makes its closest approaches to the fuel elements, where the probe either enters the element boundary layer or has its accuracy influenced by the element proximity. Although the nominal closest approach to fuel elements is 0.006 in., the probe undoubtedly touched many elements.)

More apparent influence of the spiral wires is shown by the marked similarity of the Figs. 9 and 10 traverses and of the Figs. 11 and 12 traverses. These similarities occurred even though, as the figures indicate, the Figs. 10 and 12 traverses were made quite a distance, axially, from the Figs. 9 and 11 traverses and were orientated 120° from them. The only geometrical similarity between the conditions of the upstream and the downstream sets of traverses was that the relative orientation of the spiral spacer wires was the same for both. This indicates that the overall velocity profile in the subassembly more or less spirals from one end of the core section to the other, at the same rate as the spacer wires.

The higher velocities near the right walls of the Figs. 9 and 10 plots and the lower velocities near the left walls (part of the similarity already mentioned) may be explained in terms of the different distances of the probe tip from the spacer wires at each edge.

The edge-location velocities of Figs. 10 and 12 are somewhat lower, in general, than the center-location velocities of Figs. 9 and 10, a situation that was not expected, because the equivalent diameter of the edge passages is substantially greater than that of the central passages. Possibly this phenomenon occurred because on the sides the edge-location traverses were made, the spiral spacer wires are fairly close upstream from the probe tip, and the wall prevents fluid from filling in downstream from the wires as readily as it can at the center locations. Therefore, the opposite side of the subassembly an edge-position traverse should indicate higher velocities. This potential imbalance cancels out on going from one end of the spiral wires to the other.

During all measurements reported here, a "flow straightener" (shown in Fig. 14) was inserted in the model subassembly at the core exit. This device,

112-108

Fig. 14. Flow Straightener

ft/sec value mentioned earlier as indicated by orifice measurements. This discrepancy is probably due to the fact that the traverse was made in an axial plane that intercepted no fuel elements; a traverse made in an axial plane that did intercept fuel elements would logically indicate lower velocities. The reduced velocities next to the subassembly wall are probably due to relatively high ridges in the subassembly wall, between the downstream ends of the spacer wires and the downstream ends of the fuel elements. An edge-location traverse at the core exit indicated practically zero velocities at all positions; therefore no figure has been prepared to illustrate those data.

As indicated in Section I.B, the coolant velocities presented here are of no actual use until they are used to establish the coolant flowrate associated with each fuel element. To do this, the average velocity related to each element was calculated first in the following arbitrary manner: The probe traverse path was divided into equal segments, each about 0.080 in. long and four to each fuel element, as indicated in Fig. 15. Of the four segments associated with each element, the two inner ones lie closer to the element than the two outer ones. The average velocity measured along each of the two inner segments was weighted by a factor of one; the average velocity measured along each of the two outer segments was weighted by a factor of two. Weighting was done in this manner because the readings along the two inner (and closer) segments would be more likely to suffer from mutual-interference effects between probe and element than readings along the two outer segments.

not present in the prototype, was installed to ensure that the experiment would produce some fairly reliable velocity measurements at the core exit. The pressure drop across the straightener was relatively quite small and should not have affected measurements elsewhere. The straightener was $3/4$ in. long, and the gaps between fuel-element tips, straightener, and core-exit probe tips were $1/16$ and $1/4$ in., respectively; therefore the core-exit probe tips were $1\frac{1}{16}$ in. downstream from the fuel-element tips. Figure 13 indicates that the average of the velocities found from the center-location traverse at the core exit is higher than the 6.2-

Fig. 15. Method of Dividing Traverse Path

The two weighted velocities were then averaged to obtain a value that was henceforth considered to be the average coolant velocity associated with that element.

The preferred method for calculating average velocities is to use the expression

$$\bar{v} = \frac{\int_A v dA}{A_T},$$

where A is the flow area. However, only a small fraction of the total flow area surrounding any individual fuel element was actually traversed at any axial position, because of the great difficulty of installing probes that could be moved at will. As a result, the preferred method seemed unrealistic and we used the method described above, which amounts to using the analogous expression

$$\bar{v} = \frac{\sum_L v_i w_i \Delta y}{\sum_L w_i \Delta y},$$

where w_i is the weighting factor, y is the transverse distance, and L is the entire transverse path length assigned to the fuel element. Other methods of assigning average velocities to each fuel element were also used, all methods yielding about the same results. Also, the procedure used assumes that all measured velocities have directions parallel to the subassembly axis, an assumption that is reasonable if consideration is given to the preceding discussion of yaw effects.

Fig. 16. Average Velocities Associated with Individual Fuel Elements

Figure 16 is a plot of the average velocities for the different fuel elements, determined by the foregoing method from the local velocities of Figs. 9 and 10. In calculating these averages, we used only those points connected by the solid lines in Figs. 9 and 10. As indicated by the legends, those points were obtained during a minimum number of individual traverses and were thus felt to possess a consistency that all the points combined

would lack. In many instances, scatter of data from different traverses can be explained entirely by the combination of steep velocity gradients and possible inaccuracy (0.015 in.) of probe locations.

In Fig. 16 and others, Hexagonal Row 6 refers to the 30 fuel elements in the outermost hexagonal row of elements in the subassembly, and Hexagonal Row 2 refers to the six elements in the hexagonal row surrounding the most central element. Two values are assigned to the Hexagonal Row 6 elements because the inner half of each of those elements is adjacent to a flow area identical to that of the internal elements, and the outer half is adjacent to a completely different type of flow area. The Hexagonal Row 6 velocities plotted toward the outside pertain to the outer flow areas, and those plotted toward the inside pertain to the inner flow areas.

Because of the aforementioned evidence that the overall velocity profile in the subassembly spirals from one end of the core section to the other, it was considered justifiable and/or necessary to average the Fig. 16 values for each hexagonal row of elements to obtain new average velocities (plotted in Fig. 17) applicable to typical elements in any hexagonal row of elements.

Fig. 17. Average Velocities Associated with Typical Fuel Elements in Any Hexagonal Row of Elements

Table II shows how we calculated the desired (see Section I.B) ratios of the flowrate per fuel element in any hexagonal row to the overall average flowrate per element. The Fig. 17 average velocities (Column 2) were multiplied, respectively, by the hexagonal-row flow areas (Column 3) associated with the Fig. 17 velocities, to obtain the total flowrate per hexagonal row (Column 4). The Hexagonal Row 1, or central, element is assumed to have the same average velocity as the Hexagonal Row 2 ele-

ments. The flowrate per element in each hexagonal row (Column 5) was found by dividing the Column 4 values by the number of fuel elements in the respective row. The overall average flowrate per element in the model subassembly was $1.51934/91$ or $0.01670 \text{ ft}^3/\text{sec}$, where 1.51934 is the total subassembly flowrate (Column 4), and 91 is the total number of fuel elements in the subassembly. Therefore, the Column 5 values were divided by 0.01670 to obtain the desired flowrate ratios given in Column 6 and in Fig. 18. As explained in Appendix A, these ratios can be expected to pertain to the prototype as well as to the model.

TABLE II. Calculation of Fig. 18 Flowrate Ratios

(1) Fuel-element Hexagonal Row No.	(2) Average Row Velocity (Fig. 17), ft/sec	(3) Row Flow Area, 10^{-3} ft^2	(4) Total Row Flowrate, ft^3/sec	(5) Flowrate per Element, ft^3/sec	(6) Ratio of Flowrate per Element to Overall Average Flowrate per Element
1	13.88	$1(0.9978) = 0.998$	0.01385	0.01385	0.830
2	13.88	$6(0.9978) = 5.987$	0.08310	0.01385	0.830
3	14.04	$12(0.9978) = 11.975$	0.16812	0.01401	0.839
4	13.80	$18(0.9978) = 17.962$	0.24787	0.01377	0.825
5	13.60	$24(0.9978) = 23.949$	0.32571	0.01357	0.813
6 (inner)	13.61	$14(0.9978) = 13.970$	0.19014	0.02269	1.359
6 (outer)	14.56	33.691	0.49054		
Total		108.532	1.51934

Fig. 18. Ratios of Flowrate per Typical Fuel Element in Any Hexagonal Row of Elements to Overall Average Flowrate per Element

velocity in the subassembly. However, since the outer halves of the Hexagonal Row 6 fuel elements have associated with them comparatively high flow areas and velocities, the flowrates for most of the interior elements are as much as 18% lower than the overall average flowrate per element (see Fig. 18).

An attempt was made to calculate the approximate subassembly velocity distribution in this manner: A hexagon was drawn through the centers of all fuel elements in Hexagonal Row 6. (This division line was chosen because of the identity of all flow areas inside the hexagon.) Then the equivalent diameters were calculated for the flow areas inside and outside the hexagon. Taking the square root of the ratio of these two equivalent diameters and including the effect of friction-factor variations, in accordance with the Darcy equation,⁶ we calculated the average velocity inside the hexagon to be 0.87 times the average velocity outside the hexagon. However, by weighting the Fig. 17 velocities with applicable flow areas, we found the average velocity inside the hexagon to be 0.94 times the average velocity outside the hexagon. Two possible reasons for this 8% discrepancy between calculated and experimental results are: (1) The calculated factor (0.87) does not consider the effect of the ridges on the inside of the hexagonal-tube wall. (2) The Darcy equation does not account for the presence of the spiral spacer wires.

According to the Table II values, the average velocity in the core section of the model subassembly was $1.51934/0.108532 = 14.0$ ft/sec, where 1.51934 is again the total subassembly flowrate and 0.108532 is the total subassembly flow area (Column 3). The corresponding mass flowrate was $1.51934/0.01634 = 93.0$ lb/sec, where 0.01634 is the specific volume, in ft³/lb, of the fluid. These velocity and flowrate values are in good agreement with the 13.8-ft/sec and 91.5-lb/sec values given earlier as having been obtained by orifice measurements. The values from the probe measurements may be somewhat high because of the turbulence effect noted above.

Figure 17 indicates that no fuel element has associated with it an average velocity that is more than 2% lower than the overall average

V. STATIC-PRESSURE MEASUREMENTS

Static-pressure measurements were made at various positions on the model for these reasons: (1) Knowledge of the pressure drop across the fuel-element bundle alone will be useful if core subassemblies with different blanket or fuel sections are used in the future. (2) Knowledge of the effect

of spiral spacing wires on pressure drop is useful basic information. Figure 19 shows the axial positions at which the measurements were made. With one exception, the taps were located at the centers of the faces of the hexagonal tube.

Fig. 19. Locations of Static-pressure Taps

position may not necessarily have represented the effective static pressure at that position. This is why no serious efforts were made to determine velocities by measuring local total pressures only, and using those pressures, together with static pressures measured at the walls, in Eq. 1.)

The "calculated" line in Fig. 20 was developed by using an equivalent diameter of 0.0254 ft (as calculated for the flow area of the spiral-wire region by conventional methods), a relative roughness of 0.0002, a friction factor of 0.0193 for a flowrate of 107 lb/sec ($N_{Re} = 94,500$), and a friction factor of 0.0220 for a flowrate of 53.7 lb/sec ($N_{Re} = 47,600$).⁶ If the $(p_b - p_c)$ and $(p_g - p_h)$ values are considered representative (which is recommended), then the measured values are about 30% higher than the calculated values. Such a discrepancy is to be expected because the spiral wires must cause more pressure drop than the straight wires assumed implicitly in the calculations.

The pressure drop through 1 ft of the spiral-wire region was also calculated by de Stordeur's⁷ method, which uses the concept of spacer drag coefficients. His method yields values that are very close to the above-recommended experimental values. At first glance, this is not surprising,

Fig. 20

Static-pressure Differentials across One Axial Foot of Fuel-element Section. (The subscripts used here refer to the locations shown in Fig. 19.)

since he used these experimental data when developing his drag-coefficient points. However, the EBR-II drag-coefficient point in his Fig. 2 lies well above his recommended drag-coefficient curve which was used in the "second calculations" reported in the first sentence of this paragraph. Therefore, the "second calculations" would be expected to indicate lower pressure drops than measured. Probably the reason this did not occur is that a relative roughness of 0.0002 was used to determine friction factors in the "second calculations," while de Stordeur apparently assumed perfectly smooth surfaces when calculating his drag-coefficient points.

Figure 21 is a plot of static-pressure differentials measured from the taps between the lower blanket exit and the core entrance (a and f) to the taps 12.33 in. downstream from the core entrance (b and g). The difference between the two measured curves is due almost entirely to the pressure differential (indicated by the dashed line in Fig. 21) that occurred between the two core-entrance taps a and f. The differential between these taps again could have been due to the velocity-head component problem noted above. In addition, only a small space exists between the lower blanket exit and the core entrance, and to reach that space it was necessary to drill not only through the wall of the subassembly hexagonal-tube wall, but also through the wall of the fuel-element grid, which was not necessarily in contact with the hexagonal-tube wall. As a result, pressure taps a and f could have been less than ideal.

The "calculated" line in Fig. 21 was developed by summing the calculated pressure drops through (a) 10.10 in. of the spiral-wire region, based on the "calculated" values of Fig. 20, (b) a contraction from the flow area (31.69 in.²) upstream from the lower grid plate to the flow area (28.59 in.²) within the grid-plate bars, (c) another contraction from the flow area within the grid-plate bars to the flow area (12.21 in.²) within the grid-plate bar heads, and (d) an expansion from the flow area within the grid-plate bar heads to the flow area (15.62 in.²) within the spiral-wire region.

Figure 22 is a plot of static-pressure differentials measured from the taps 16.67 in. upstream from the core exit (d and i) to the taps at the core exit (e and j). Discrepancies between measured values are due almost entirely to the pressure differential (noted above) between taps d and i. The "calculated" line in Fig. 22 was developed by summing the calculated pressure drops through (a) 13.77 in. of the spiral-wire region, based on the calculated values of Fig. 20, (b) an expansion from the flow area (15.62 in.²) within the spiral-wire region to the flow area (26.74 in.²) within the

fuel-element tip region, and (c) another expansion from the flow area within the fuel-element tip region to the flow area (34.92 in.²) immediately downstream from the fuel elements.

Fig. 21

Static-pressure Differentials from Core Entrance Taps to Taps 12.33 in. Downstream from Core Entrance. (The subscripts used here refer to the locations shown in Fig. 19.)

Fig. 22

Static-pressure Differentials from Taps 16.67 in. Upstream from Core Exit to Taps at Core Exit. (The subscripts used here refer to the locations shown in Fig. 19.)

The pressure differentials ($p_a - p_e$) and ($p_f - p_j$) measured from the fuel-section inlet to the outlet have been converted into predicted values for the sodium prototype. Equations 5 and 7 (in Appendix A) were used in making the conversion. Results are given in Fig. 23. According to Seim,⁸ the "average" curve values of Fig. 23 are about 10% lower than those indicated by water tests on a prototype subassembly during various stages of assembly. At least part of this discrepancy could be due to the lower relative roughness of the model (as described in Appendix A).

The individual pressure differentials given here are not based merely on readings from manometers connected between each pair of taps involved. Rather, enough different combinations of tap and manometer hookups were used so that the differentials from the measurements could

be calculated by at least two, and in many cases three or four, independent methods. The values given are average values; in general, the extreme values obtained did not differ from the average values by more than 0.05 in. Hg under H_2O .

Fig. 23

Predicted Static-pressure Differentials across Entire Axial Length of Prototype Fuel-element Bundle

APPENDIX A

Use of a Water Fluid-flow Model to Predict
the Behavior of a Sodium Prototype

Following Murphy,⁹ the pertinent variables and their dimensions are:

Δp	Pressure drop	$ML^{-1} T^{-2}$
ℓ	Control distance	L
d	Cross-sectional dimension	L
r	Roughness of pipe	$\bar{}$
ρ	Density of liquid	ML^{-3}
μ	Viscosity of liquid	$ML^{-1} T^{-1}$
v	Velocity of liquid	LT^{-1}

(Here, M indicates mass, L indicates length, and T indicates time.)

From the Buckingham Pi Theorem, four pi terms are required. One form in which the general dimensionless equation may be written is

$$\frac{\Delta p}{\rho v^2} = f\left(\frac{d}{\ell}, r, \frac{\rho v d}{\mu}\right). \quad (2)$$

The design equations are

$$\frac{d_m}{\ell_m} = \frac{d_p}{\ell_p} \quad \text{or} \quad d_m = \frac{d_p}{n}, \quad (3)$$

$$r_m = r_p, \quad (4)$$

and

$$\frac{\rho_m v_m d_m}{\mu_m} = \frac{\rho_p v_p d_p}{\mu_p}; \quad (5)$$

and the prediction equation is

$$\frac{\Delta p_p}{\rho_p v_p^2} = \frac{\Delta p_m}{\rho_m v_m^2}, \quad (6)$$

which can be restated, using Eqs. 3 and 5, as

$$\Delta p_p = \left(\frac{1}{n}\right)^2 \left(\frac{\mu_p}{\mu_m}\right)^2 \frac{\rho_m}{\rho_p} \Delta p_m \quad (7)$$

and, using either Eqs. 6 and 7 or 3 and 5, as

$$v_p = \frac{1}{n} \frac{\mu_p}{\mu_m} \frac{\rho_m}{\rho_p} v_m. \quad (8)$$

From Eq. 8, if design Eqs. 3-5 are satisfied, prototype velocities are proportional to model velocities. Satisfying Eq. 3 ensures that flow areas in the prototype are also proportional to those in the model, so that flow-rates in the prototype are likewise proportional to those in the model.

In constructing the model, we made no effort to obey design Eq. 4. As Murphy states, that equation requires that the relative roughness s/ℓ (where s is any distance associated with irregularities in the flow channel surfaces) be the same in both model and prototype. However, s/ℓ of the prototype is about 0.0006; that of the model was about 0.0002. According to Moody,⁶ at the Reynolds Numbers associated with the model and prototype, the friction factor for a relative roughness of 0.0006 is about 0.021, and that for 0.0002 is about 0.020; this gives an indication of how disobeying Eq. 4 can affect pressure-drop predictions. Although it is unfortunate that this sort of discrepancy existed, we do not really believe that artificially roughening the model would have improved the accuracy of the velocity measurements enough to justify the time and money required.

APPENDIX B

Estimate of Effects of Coolant Mixing and of Transverse
Heat Transfer on Coolant Temperatures

As indicated in Section IV of this report, the outer halves of the exterior (Hexagonal Row 6) fuel elements in the EBR-II core-type subassembly have associated with them comparatively large coolant flow areas and velocities. Therefore, the coolant flowrates for most of the interior elements are as much as 18% lower than the overall average flowrate per element. If we assume that no coolant mixing or transverse heat transfer occurs between element rows in the subassembly, the coolant temperature rise associated with each element is

$$\Delta T_{c,N} = \frac{\int_0^{L_f} q'_n(x) dx}{\dot{m}_{nc}} = \frac{\int_0^{L_f} q'_{\max,n} \sin \frac{\pi(x+S)}{L_f + 2S} dx}{\dot{m}_{nc}}. \quad (9)$$

(This equation results because the axial power distribution in EBR-II core may be closely approximated by a chopped sine curve.) As Eq. 9 shows, the coolant temperature rises for the interior, low-flowrate elements can be substantially higher than those for the exterior, overcooled elements. This situation is undesirable because the maximum permissible power output of the reactor is limited by the maximum permissible fuel (and thus coolant) temperatures. Because of the undercooled interior elements, the maximum permissible power is therefore lower than it would be if all elements in the subassembly were exposed to the same flowrate.

This imbalance of coolant flowrates would not be harmful if coolant mixing and transverse heat transfer between element rows in the subassembly were sufficient to carry significant amounts of heat from the hotter coolant around the interior elements to the colder coolant around the exterior elements. This appendix discusses efforts made to estimate the beneficial effects, if any, of mixing and transverse heat transfer in the EBR-II core-type subassembly. In making this estimate, we found the work of Shimazaki and Freede¹⁰ to be an informative reference.

First, fuel-element Hexagonal Rows 2-6 were considered to be divided into five channels as indicated in Fig. 24. Then the following heat balance was written for each channel:

$$\frac{dT_N}{dx} = \frac{q'_N + (C' + \dot{m}'c)_{N+1,N} (T_{N+1} - T_N) - (C' + \dot{m}'c)_{N,N-1} (T_N - T_{N-1})}{\dot{m}_{nc}}, \quad N = 2, 3, 4, 5, 6, \quad (10)$$

where

$$\dot{m}'_{N+1,N} = \frac{K}{2} (\dot{m}_{N+1} + \dot{m}_N),$$

$$\dot{m}'_{N,N-1} = \frac{K}{2} (\dot{m}_N + \dot{m}_{N-1}),$$

$$q'_N = q'_{\max,N} \sin \frac{\pi(x+S)}{L_f + 2S},$$

$$C' = \frac{k_{\text{eff}} \bar{P}}{\Delta r},$$

$$\Delta r = A/\bar{P},$$

$$\bar{P} = 6(D_{FE} + D_W)(N_R - 1\frac{1}{2}),$$

$$A = \frac{3\sqrt{3}}{2} (D_{FE} + D_W)^2 [(N_R - 1)^2 - (N_R - 2)^2],$$

and

$$k_{\text{eff}} = k_U V_U + k_{Na} V_{Na} + k_{SST} V_{SST}.$$

112-2317

Fig. 24. Division of Subassembly into Channels
(First Method)

stainless steel tube. With water flowing through the subassembly at velocities (and Reynolds Numbers) similar to those in the prototype, dye was injected through a 0.032-in.-ID tube discharging into the inlet end of

(In the expression for k_{eff} , the volume fractions apply only to the materials inside the hexagonal subassembly tube. The value used for k_{Na} was 20% larger than that for stagnant sodium, to take into account the eddy diffusivity of the turbulent sodium.¹¹)

Before Eq. 10 can be solved, K , a mixing coefficient, must be determined. To do this, we considered results from an experiment conducted by Carrier¹² several years ago. He made use of the core section of a full-scale EBR-II core-type subassembly, which employed a plastic tube to enclose the fuel elements rather than the usual

the core section near the center of the subassembly. At that time, a photograph (Fig. 25) was made of the downstream ends of the fuel elements;

measurements on this photograph indicated that the dye had spread over a diameter of 1.40 in. by the time it had passed from the inlet of the core section to the exit.

To use this meager information to evaluate K , we first used the Bouguer-Beer law,¹³ which states that the amount of light passing through the dye-darkened water in the plastic-housed model subassembly at the core-region exit, and from which the Fig. 25 photograph was made, was a function of the dye concentration; namely,

$$\log \frac{I}{I_0} = -abC. \quad (11)$$

111-3613

Fig. 25. Dye Distribution at Exit of Core Section

A positive transparency was made of the photograph, and then a Welch Densichron Photometer, Model 2150, was used to determine the way $\log (I_i/I)$ varied in the transparency from one side of the subassembly to the other, immediately downstream from the downstream ends of the fuel elements. (Actually, only the central portion of the transparency was traversed, since the light passing through the left and right sides of the plastic tube was affected by the sloping hexagonal inner surface of the tube.) Next, the constant value observed for $\log (I_i/I_0)$ outside the region darkened by the dye was subtracted from all measurements, producing $\log (I_0/I)$ values that indicated quite well the light attenuation due to dye-darkened water only and not due to plastic tube, clear water, and transparency. Finally, the $\log (I_0/I)$ values were normalized into the form $\log (I/I_0)/[\log (I/I_0)]_{\max}$; variation of these values with radial position is shown by the open squares in Fig. 26.

112-6281

Fig. 26. Variation of Dye-caused Light Attenuation with Radial Position at Core-section Exit

The other step taken to estimate K was to again consider

112-2318

Fig. 27. Division of Subassembly into Channels (Second Method)

Runge-Kutta-Gill method, as programmed for the LGP-30 digital computer by Thacher,¹⁴ was used. Table III gives the constants, in arbitrary units, used in Eq. 12. Figure 28 shows how the dye concentration was calculated to vary from the entrance of each channel to the exit for a K of 1.7; Fig. 29 shows the calculated radial distribution of dye concentration at the core-section exit for several values of K .

For each value of K , the dye concentrations calculated to exist at the core-section exit were converted into light attenuation terms as follows: The light that passed through the circle of dye-darkened water at the

the subassembly to be divided into five channels, this time as indicated in Fig. 27. Then the following dye-concentration balance was written for each of the five channels:

$$\frac{dC_n}{dx} = \frac{\dot{m}'_{n+1,n}(C_{n+1} - C_n) - \dot{m}'_{n,n-1}(C_n - C_{n-1})}{\dot{m}_n},$$

$$n = 1, 2, 3, 4, 5, \quad (12)$$

where each $\dot{m}'$ is defined in the same manner as for Eq. 10.

The five simultaneous equations were integrated, for various values of K , over the length of the core section; for this purpose, the

TABLE III. Constants for Eq. 12

n	$\dot{m}_n$	C_n at $x = 0$
1	1	1
2	6	0
3	12	0
4	18	0
5	24	0

112-6271

Fig. 28. Calculated Variation of Dye Concentration with Axial Position in Core Section

112-6282

Fig. 29. Calculated Variation of Dye Concentration with Radial Position at Core-section Exit

core-section exit was assumed to pass along a chord of length

$$b = 2[(0.83)^2 - r^2]^{1/2},$$

where 0.83 is the approximate mean distance, in inches, from the center of Channel 1 to the center of Channel 5 (see Fig. 27). This is the largest distance for which dye concentrations were calculated, since Channel 5 borders several walls of the hexagonal subassembly tube. (A condition used in the calculations was that Channel 5 was isolated from the external surroundings.) The average concentration along this path was taken to be equal to $(C_r + C_{0.83})/2$, which is what it would be if the dye concentration varied like a cosine curve does from minus π to plus π . Figure 29 indicates the concentration does indeed vary in such a manner. Equation 11 then was modified to

$$\frac{\log(I/I_0)}{[\log(I/I_0)]_{\max}} = \frac{(C_r + C_{0.83}) b/2}{[(C_r + C_{0.83}) b/2]_{\max}}. \quad (13)$$

From Eq. 13, values of the log ratio were calculated using the C_r and $C_{0.83}$ values of Fig. 29. These values are plotted in Fig. 26 for comparison with the photometrically determined values. On the basis of this comparison, we concluded that a K of about 1.1 appears to do the best job of describing the coolant mixing within the core section of the EBR-II core-type subassembly.

Obviously, the foregoing procedure did not take into account a number of things, e.g., local variations in K , wavelength sensitivity of both films and photometer, and use of formal integration to determine $\log(I/I_0)$ from Eq. 11. Therefore, the results are considered to be more significant from a qualitative, than from a quantitative, standpoint. A comparison of this K value with others obtained by different methods in various configurations has been published.¹⁵

Using a K of 1.1 and the additional constants listed in Table IV, we integrated Eq. 10 over the length of the fuel section, again using the Runge-Kutta-Gill program. The Table IV values apply, at rated reactor power, to the hottest subassemblies in the fourth row of subassemblies away from the reactor vertical centerline; it is in these subassemblies that the maximum fuel and cladding temperatures occur.

Local power variations in each channel were not taken into account in these calculations. (To do so would require investigating about 45 channels instead of five.) The largest variations occur in the Hexagonal Row 6 Channel, where the local power densities at the locations nearest the reactor vertical centerline are about 16% higher than they are at the locations farthest

from that centerline. Therefore, the results indicate average rather than local conditions in each channel. Another condition used in the calculations was that the Hexagonal Row 2 Channel was isolated from the Hexagonal Row 1 Channel and the Hexagonal Row 6 Channel was isolated from the surroundings outside the subassembly.

TABLE IV. Constants for Eq. 10

c	$0.305 \text{ B/lb}_M \text{ } ^\circ\text{F}$	L	1.185 ft
D_{FE}	0.01450 ft	S	0.466 ft
D_W	0.004083 ft	$T_{x=0}$	$0 \text{ (for all } N)$
k_{Na}	$0.0135 \text{ B/sec-ft}^2\text{ } ^\circ\text{F}$ (see text)	V_{Na}	0.471
k_{SST}	$0.00350 \text{ B/sec-ft}^2\text{ } ^\circ\text{F}$	V_{SST}	0.153
k_U	$0.00550 \text{ B/sec-ft}^2\text{ } ^\circ\text{F}$	V_U	0.376
K	1.10		

N	$\dot{m}_N, \text{ lb}_M/\text{sec}$	$q'_{\max, N}, \text{ B/sec-ft}$
2	0.601	57.6
3	1.210	115.4
4	1.785	173.0
5	2.342	230.5
6	4.890	288.2

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Fig. 30. Calculated Variation of Coolant Temperature Rise with Hexagonal Row Number at Core-section Exit

Figure 30 shows what the coolant temperature rises were calculated to be for the five channels. Values that include no mixing or transverse heat transfer between channels, as calculated with Eq. 9, are included for comparison. As can be seen, when mixing and transverse heat transfer are included, the coolant temperature rise in the Hexagonal Row 3 Channel is only about 1% lower than what it would be if there were no mixing and transverse heat transfer. On the other hand, the rise in the Hexagonal Row 5 Channel is about 17% lower on the same basis.

(When a K of 1.7 was used in the calculations, the rises in Hexagonal Rows 3 and 5 were about 4 and 19% lower, respectively, than if there were no mixing or transverse heat transfer.) Even though these findings are qualitative, the following conclusions appear to be valid:

1. The Hexagonal Rows 5 and 6 Channels are so well cooled, comparatively, that even though some of the fuel elements in those rows produce as much as 8% more power than an average element in the subassembly, the maximum fuel and cladding temperatures in the subassembly will occur in the fuel elements of one of the other channels.

2. The coolant temperature rises in the Hexagonal Rows 1-4 Channels can justifiably be calculated on the basis that no mixing or transverse heat transfer occurs between channels. This procedure is conservative, according to the admittedly qualitative results reported above, but not so conservative that its use results in lowering the maximum permissible reactor power an unreasonable amount.

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