COOLANT MIXING IN LMFBR ROD BUNDLES AND
OUTLET PLENUM MIXING TRANSIENTS

Progress Report

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ABSTRACT

MEASUREMENT OF HEAT AND MOMENTUM EDDY DIFFUSIVITIES
IN RECIRCULATING LMFBR OUTLET PLENUM FLOWS

by
Vincent P. Manno
and
Michael W. Golay

An optical technique has been developed for the measurement of the eddy diffusivity of heat in a transparent flowing medium. The method uses a combination of two established measurement tools: a Mach-Zehnder interferometer for the monitoring of turbulently fluctuating temperature and a Laser Doppler Anemometer (LDA) for the measurement of turbulent velocity fluctuations. The technique is applied to the investigation of flow fields characteristic of the LMFBR outlet plenum. The study is accomplished using air as the working fluid in a small scale Plexiglas test section. Flows are introduced into both the 1/15 scale FFTF outlet plenum and the 3/80 scale CRBR geometry plenum at inlet Reynolds numbers of 22,000.

Measurements of the eddy diffusivity of heat and the eddy diffusivity of momentum are performed at a total of 11 measurement stations. Significant differences of the turbulence parameters are found between the two geometries, and the higher chimney structure of the CRBR case is found to be the major cause of the distinction. Spectral intensity studies of the fluctuating electronic analog signals of velocity and temperature are also performed. Error analysis of the overall technique indicates an experimental error of 10% in the determination of the eddy diffusivity of heat and 6% in the evaluation of turbulent momentum viscosity. In general, it is seen that the turbulence in the cases observed is not isotropic, and use of isotropic turbulent heat and momentum diffusivities in transport modelling would not be a valid procedure.
Reports Issued Under This Contract


E. Khan, W. M. Rohsenow, A. Sonin, N. Todreas, "Input Parameters to the ENERGY Code (To be used with the ENERGY Codes Manual), COO-2245-17TR, MIT, May 1975.


P. Carajileskov and N. Todreas, "Experimental and Analytical Study of Axial Turbulent Flows in an Inner Subchannel of a Bare Rod Bundle", COO-2245-19TR, MIT.

B. Topical Reports (Continued)


B. **Topical Reports (Continued)**


S. Glaser, N. Todreas, W. Rohsenow, and A. Sonin, "TRANSENERGY S,M - Computer Codes for Coolant Temperature Prediction in LMFBR Cores During Transient Events", To be issued as COO-2245-52TR, MIT.

B. Topical Reports (Continued)


K. Basehore and N. E. Todreas, "SUPERENERGY: Multiassembly Thermal-Hydraulic LMFBR Code", To be issued as Topical Report COO-2245-57TR, Department of Nuclear Engineering, MIT.


Hafeez Khan, Chong Chiu and Neil Todreas, "Laboratory Manual for Salt Mixing Test in Rod Bundles", To be issued as Topical Report Coo-2245-62TR, Department of Nuclear Engineering, MIT.
C. Papers and Summaries


Reports Issued Under this Contract

C. Papers and Summaries (Continued)


COOLANT MIXING IN LMFBR ROD BUNDLES AND OUTLET PLENUM MIXING TRANSIENTS

Contract AT(11-1)-2245

Quarterly Progress Report

The work of this contract has been divided into the following Tasks:

TASK I: BUNDLE GEOMETRY (WRAPPED AND BARE RODS)

TASK IA: Assessment of Available Data
TASK IB: Experimental Bundle Water Mixing Investigation
TASK IC: Experimental Bundle Peripheral Velocity Measurements (Laser Anemometer)
TASK ID: Analytic Model Development - Bundles

TASK II: SUBCHANNEL GEOMETRY (BARE RODS)

TASK IIA: Assessment of Available Data
TASK IIB: Experimental Subchannel Water Mixing Investigation
TASK IIC: Experimental Subchannel Local Parameter Measurements (Laser Anemometer)
TASK IID: Analytic Model Development - Subchannels

TASK III: LMFBR OUTLET PLENUM FLOW MIXING

TASK IIIA: Analytical and Experimental Investigation of Velocity and Temperature Fields

TASK IV: THEORETICAL DETERMINATION OF LOCAL TEMPERATURE FIELDS IN LMFBR FUEL ROD BUNDLES
TASK I: BUNDLE GEOMETRY (WRAPPED AND BARE RODS)

TASK IB.2: Experimental Bundle Water Mixing Experiments
(Michael Pate and Hafeez Khan)

During the period of this report, most of the time was spent on the training of Michael Pate, a newcomer to the Project. He was given full training in the fabrication and platinization of the probes, as well as their locations in the flow separator housing. Considerable time was also spent familiarizing him with the Computer Operating System. During this period, another complete set of mixing data was obtained for the 12" lead bundle to check out the previous data, and also to confirm the validity of the present arrangement for locating the probes. Data was also taken for the pressure drop in the bundle. This data was not for the overall bundle pressure drop, but was for individual subchannels, i.e. central, edge, and corner.

The 6" lead rods were also prepared by wire wrapping them.
Progress in shaved-wire 61-pin blanket bundle experiment has been made in this quarter. The flow split measurements for interior, edge, and corner subchannels have been done both in laminar and turbulent flow regimes. Due to the lack of the existing data in laminar regime, most of the effort has been concentrated in this area.

The test section was made up by shaving the wire wrap spacers adjacent to hexagonal duct wall to half of their original diameter. Each face of the duct wall is then squeezed a little toward the center of the assembly so that the flow area in the peripheral subchannels is reduced while the flow areas in the interior subchannels remain the same as that for full-wire blanket bundle. The purpose of this design is to force the coolant flow toward the central region of the blanket bundle and hence, eliminate the local cladding hot spots and overall thermal gradients across the whole assembly.

Figure 1 shows the flow split parameter $x_1$ for interior subchannels as function of bundle average Reynolds number. In the laminar regime, as the flow increases, the higher pressure drop in the central region than that in the peripheral region tends to divert the coolant to the peripheral subchannels. However, this tendency gradually saturates as bundle flow becomes highly turbulent. In this region (highly turbulent), the average gain of coolant flow in the central region is about 3% over the full wire design. Detailed comparison on flow split behavior between half-wire and full-wire designs is under way.

The flow split parameter $x_2$ for edge subchannels is shown in Figure 2. As the flow increases, $x_2$ increases, as we might have expected from the conservation law, in the laminar region and finally reaches a constant value when the coolant flow is highly turbulent. The behavior of this flow split parameter in the transition ($Re \sim 5000$) region is not clear and more data is going to be taken in this specific area.

The flow split data for corner subchannels is similar to that for edge subchannels. Figure 3 shows the flow split parameter $x_3$ for corner subchannels. It increases gradually from laminar to turbulent flow and comes up to a constant value approximately equal to 1.0. This result confirms the assumption that we made previously: the flow velocity in the corner subchannels is roughly equal to the bundle average coolant velocity. But, notice that this assumption is only good for turbulent flow.
The "closed loop test" has been postponed because low coolant temperature is favorable for the present experiment which is concentrated in the laminar region. The test run will be conducted when it is necessary.
Figure 1. Interior Subchannel Flow Split Parameter $X_1$ versus Bundle Average $Re$
Figure 3. Corner Subchannel Flow Split Parameter $X_3$ Versus Bundle Average Re
TASK 1D.2: Transient Code Development  
(Stuart Glaser)

Our work on transient code development has progressed to the point of completion of the code TRANSENERGY-S. This discussion summarizes our entire effort to date in this area.

Techniques of predicting LMFBR coolant temperatures were investigated with the objective of developing a production-type computer code for use in transient safety analyses and design studies of LMFBR cores. Existing computer codes in the COBRA family are able to predict transient coolant temperatures. However, their complex models require long computation times and large amounts of core storage making them undesirable for use as production codes until accurate schemes for lumping regions are developed.

The extreme simplicity of the steady-state ENERGY model indicated that, if its approach could be extended to transient cases, then the resulting codes would be faster than existing codes and would be suitable for use as production codes. It was found that if the ENERGY model was modified to include heat capacity effects associated with the coolant and the duct wall, and if a transient model of the fuel pins could be developed, then a transient model could be developed for hexagonal wire-wrapped assemblies in which natural circulation effects are negligible.

TRANSENGY-S, a computer code based on a modified version of the ENERGY model, was developed. The code calculates coolant temperatures for a single wire-wrapped LMFBR fuel assembly during coolant flow, power and inlet temperature transients. It is based on a computational scheme similar to the one used in the SUPERENERGY code. The assembly is nodalized using the subchannel concept employed in SUPERENERGY. The energy equation for the coolant is a temporally implicit, spatially explicit finite difference equation. The fuel model is a one-dimensional implicit finite difference equation.

The predictions of TRANSENERGY-S were compared with experimental measurements and with the predictions of COBRA-III-C/MIT and COBRA-III-M. TRANSENERGY-S made accurate predictions for most transients in wire-wrapped hexagonal fuel bundles. Changes in geometry and very rapid transients were found to cause significant deviations in the predictions by TRANSENERGY-S.
The computation time and core storage required to extend TRANSENERGY-S to analysis of more than 19 assemblies make it unsuitable as a production code. A second code, TRANSENERGY, TRANSENERGY-M, has been proposed. It calculates coolant temperatures in from one to 41 coupled fuel assemblies. The computational requirements are reduced by lumping the assembly into six interior nodes, six edge nodes, six nodes in the duct wall and six nodes in the interassembly gap. The coarse nodalization of the assemblies makes TRANSENERGY-M useful for complete assemblies only. The coolant temperature predictions within the assemblies by TRANSENERGY-M are not as accurate as the detailed predictions made by TRANSENERGY-S for single assemblies. However, they are accurate enough to identify the hottest assemblies relative to the other assemblies.

This suggests a technique which may be useful for many transient analyses. Detailed core analyses may be made using a cascade approach. TRANSENERGY-M is used to determine the heat flux on each face of the fuel assemblies as a function of time. These heat fluxes are input to TRANSENERGY-S as boundary conditions for the sub-channel-level analysis of the hottest fuel assemblies indicated in the TRANSENERGY-M analysis.

Future work is expected to include indepth testing of TRANSENERGY-S in order to determine its accuracy and its limitations. Development of the proposed TRANSENERGY-M computer code is pending awaiting an assessment of ongoing other national efforts.
In work recently completed the eddy diffusivities of heat and momentum in small scale LMFBR outlet plenum flows have been measured. Each diffusivity has been measured separately in airflows, and the dimensionless ratio of these diffusivities has been computed. The purpose of these measurements is to investigate the degree to which common assumptions in the formulation of these quantities in turbulence modelling are valid.

In particular, it is commonly assumed (e.g. in the VARR-II analyses of LMFBR Outlet Plenum Flows) that the ratio of $\epsilon_H/\epsilon_M$, where $\epsilon_H$ and $\epsilon_M$ are the eddy diffusivities of heat and momentum, respectively, is constant in time and space throughout a flow transient. It is usually also assumed that the turbulence is isotropic. Our work shows that these are poor approximations for outlet plenum flows.

The experiments were performed in two-dimensional steady non-isothermal air flows. In these flows co-flowing streams of hot and cold air were introduced symmetrically at the plenum inlet, and their turbulent mixing was observed at selected stations in the flow field (see Fig. III-1). The turbulent velocity and temperature fields were measured using a laser doppler anemometer and a Mach-Zehnder interferometer, respectively. These measurements add to the relatively small literature in existence regarding turbulent heat transport, and are of special importance in outlet plenum flow modelling. Power spectral density data at a representative FFTF measurement station for velocity and temperature are shown in Figs. 2 and 3. It is seen that velocity fluctuations remain strong in the range up to 16 KHz, while significant temperature fluctuations in the range above 4KHz are not observed. The resulting measured diffusivity values are shown in Figs. 4 through 8. It is seen that overall turbulence levels decay monotonically as the inlet jet follows mean streamlines to the plenum exit, and as the flow becomes well-mixed the magnitude of the effective eddy heat diffusivity becomes progressively damped. It is also seen that the turbulence is generally anisotropic, especially down stream of the inlet chimneys.

These data provide the basis for formulation of an improved turbulence model for heat transport. However, our research has indicated the existence of basic problems in models for turbulent momentum transport. This latter class of problems must be resolved before it would make sense to formulate an
eddy heat transport model--based upon the momentum transport model.

This work is described in detail in Ref. 1, which has been distributed separately. The abstract of Ref. 1 is appended to this text.
FIGURE 1: ISOMETRIC VIEW OF MACH-ZEHNDER INTERFEROMETER

L - Laser, B - Beamsplitter, M - Mirror, D - Detector
FIGURE 2: T' FREQUENCY SPECTRUM AT STATION B IN FFTF GEOMETRY
FIGURE 3: $u'$ FREQUENCY SPECTRUM AT STATION B IN FFTF GEOMETRY
FIGURE 4: EDDY DIFFUSIVITY OF HEAT IN FFTF GEOMETRY (X-COMPONENT) (m²/sec)

Point D: 0.0037
Point E: 0.0077
Point F: 0.0005
Point C: 0.0020
Point B: 0.0019
FIGURE 5: EDDY DIFFUSIVITY OF HEAT IN FFTF GEOMETRY (Y-COMPONENT)

($m^2$/sec)
FIGURE 6: EDDY DIFFUSIVITY OF MOMENTUM IN FFTF GEOMETRY (X-COMPONENT) (m²/sec)

D
0.0175

E
0.0339

F
0.0091

C
0.0309

B
0.0160
FIGURE 7: EDDY DIFFUSIVITY OF MOMENTUM IN FFTF GEOMETRY (Y-COMPONENT)

\((m^2/sec)\)

- **D**
  - 0.0482

- **E**
  - 0.0868

- **F**
  - 0.0229

- **C**
  - 0.0434

- **B**
  - 0.0964
### TABLE 1: \( \frac{\varepsilon_H}{\varepsilon_M} \) \text{x} In the FFTF Geometry

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<th>Position</th>
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<tr>
<td>C</td>
<td>.065</td>
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<tr>
<td>D</td>
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<tr>
<td>E</td>
<td>.227</td>
</tr>
<tr>
<td>F</td>
<td>.055</td>
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</tbody>
</table>

### TABLE 2: \( \frac{\varepsilon_H}{\varepsilon_M} \) \text{y} In the FFTF Geometry

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<td>F</td>
<td>.039</td>
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TASK IV: THEORETICAL DETERMINATION OF LOCAL TEMPERATURE
FIELDS IN LMFBR ROD BUNDLES

TASK IV.A: Coupling of Lumped and Distributed Parameter Codes
(Man-Kit Yeung)

In the previous report, a computational scheme of coupling
the distributed and lumped parameter solution methods was pro-
posed. The general flow chart of this scheme is shown in
Figure 1. It has been reported that the results for the
7-rod bundle calculation were quite encouraging. More
detailed results of this 7-rod bundle calculation can be found
in the forthcoming topical report by Yeung and Wolf [1].

During the last quarter, a 19-rod bundle calculation has
been performed and compared with the measurement of Möller
and Tschöke's experiment [2] in order to demonstrate the
capability of this analysis. The geometric configuration and
the subchannel layout of the bundle is shown in Figure 2. The
mixing parameters used for the COBRA-IIIC calculation are those
presented in [3]. As shown in Figure 2, by the shaded region,
a 7-rod section has been cut out from the 19-rod bundle. In
order to obtain a reasonable estimate for the clad tempera-
ture of the corner rod, it is necessary to have some knowledge
of the coolant temperatures of the two shaded areas. Unfor-
tunately, these are not directly available from the COBRA-IIIC
calculations. Approximations have to be made to obtain
these values. In this case, it has been assumed that the
average temperature of the shaded area which consists of
$<A_3>3$ and half of $<A_2>_3$ is equal to $T_3$, the coolant tempera-
ture of subchannel 3. This approximation should be reason-
ably valid as long as a large power gradient does not exist across
the bundle. On the other hand, it would be questionable
to use $T_4$ as the temperature of the shaded area $<A_4>_4$ because
of the geometric irregularity of subchannel 4. One possible
way to determine $<T_4>_4$ is to utilize the calculational
results of the fully developed temperature field of the 19-rod
bundle. In addition, the axial correction $F(Z)$ is also
used to account for the thermal entrance effect. Thus, the
relation between $<T_4>_4$ and $T_4$ is given by the following:

$$
\frac{<T_4>_4 - T_4}{\bar{q}''''/a^2/2kC} = \frac{<A_3>_4}{<A_3>_4 + <A_4>_4} \{2 \gamma_0142 + F(Z) [<\Theta_4>_4 - <\Theta_3>_3] \}
$$

Having established the values of the coolant temperatures neces-
sary for the analysis for the 7-rod section, the coupled
distributed-lumped parameter technique is applied at the axial
position of $Z = 25.94''$ and the result of calculation is shown
in Fig. 3 in which the clad temperature distribution of the corner cell is plotted vs. the angular position $\phi$. For comparison, the clad temperature distribution of the corner cell measured by Möller and Tschöke is also presented. It can be seen that the shape of the predicted curve compares fairly well with the experiment. The clad temperature distribution predicted by the coupled analysis matches the experimental data in the near-wall region fairly well but tends to over-estimate the temperature in the away-from-wall region. In addition, the predicted clad temperature distribution has a maximum (minimum) circumferential variation of approximately $50^\circ F$ which is about $16^\circ F$ higher than the measurement. The discrepancy between the calculated and the measured clad temperature distribution may be the combined effect of the following uncertainties and derivations between experiment and calculation:

(1) Uncertainty in the geometric and thermal parameters of the experimental apparatus.

(2) The rounded corner of the bundle shroud in the experimental apparatus.

(3) Underestimation of the mixing parameter.

Among these three possibilities, it is felt that the mixing parameter probably has the greatest impact on the result of the calculation.

Therefore, it becomes apparent that the determination of a realistic clad temperature distribution relies on the validity of the subchannel calculation which cannot be achieved without the proper knowledge of the various mixing parameters. As a result, the present analysis is only a first step effort to link the distributed and lumped parameter analysis and improvements in many areas will be made to extend and upgrade the analysis.
Fig. 1. General Procedure of the Coupled Distributed-Lumped Parameter Analysis
Fig. 2. Subchannel Layout of the 19-Rod Bundle
Fig. 3  Clad Temperature Distribution of the Corner Rod for the Experimental 19-Rod Bundle
The work on the turbulent heat transfer analysis of hexagonal rod bundles in LMFBRs has been underway throughout this quarter. The computer code RODBUN by Ramm [4] has been made operational and employed for various analyses. Thus far, the principal work has been the set-up of the code and an attempt to initiate the code to run in an efficient way.

The code RODBUN uses the finite differencing scheme to solve the conservation equations of momentum and energy for an incompressible, steady-state, hydrodynamically fully developed turbulent flow for an equilateral triangular rod array. A phenomenological turbulent model, based on the principal ideas of Buleev's theoretical model of turbulent transfer in a three dimensional fluid flow [5], is used in the code. Moreover, the code can handle any anisotropic turbulent diffusivity and also the effect of secondary flow.

Substantial time and effort have been spent to fix up the code and have it run well because quite a number of errors have been detected in the original version of the code due to various reasons. First, an initiation for the arrays in the common blocks and many of the unknown variables has been implemented. Second, several statements have been changed because they read variables from a scrap tape that is not needed at M.I.T.. Third, a minor re-adjustment of the iteration loop is performed, thus making sure that there won't be any needless iterations. Furthermore, a graphic display of the velocity fields has been added to the code.

By solving the momentum equation, the velocity field of the internal cell can be obtained and plotted in a three-dimensional way as shown in Fig. 4 and Fig. 5. Since the underlying basic principle assumes that the flow is hydrodynamically fully developed, the velocity profile stays the same at any of the axial levels. Obviously, with the addition of the secondary flow, the azimuthal gradient of the axial velocity becomes comparatively smaller. Finally, the typical time to run the code in order to obtain the velocity profile requires less than 30sCPU time totally in an IBM370-168 machine, which is amazingly good. Next, with the result of this velocity distribution, the temperature field of the internal cell will be calculated and shown in the next quarter report.
Fig. 4 Velocity Profile of the Internal Cell with Anisotropic Turbulent Model and No Secondary Flow

(Boundary Condition: P/D = 1.05, Dia. = 0.622 in., Re = 3.40 x 10^4, Hydrodynamically Fully Developed, \( V_{\text{avg.}} = 10.3 \text{ ft/sec.} \))
Fig. 5  Velocity Profile of the Internal Cell with Anisotropic Turbulent Model and Secondary Flow

(Boundary Condition:  P/D = 1.05, Dia. = 0.622 in.,
Re = 3.39 \times 10^4, Hydrodynamically Fully Developed,
V_{avg.} = 10.14 ft/sec.)
REFERENCES


