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AEC RESEARCH AND DEVELOPMENT REPORT
JULY 1973

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LMFBR IN-CORE INSTRUMENTATION AND IN-VESSEL SURVEILLANCE SYSTEMS

SIXTH QUARTERLY REPORT
APRIL-JUNE 1973

U.S. ATOMIC ENERGY COMMISSION
CONTRACT AT(04-3)-893 TASK 9

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UC-79m
AEC Research and
Development Report
July 1973

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**LMFBR IN-CORE INSTRUMENTATION AND IN-VESSEL
SURVEILLANCE SYSTEMS
SIXTH QUARTERLY REPORT
APRIL — JUNE 1973**

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ABSTRACT

This is the sixth in a series of reports covering the development of instrumentation for surveillance of the core and in-vessel components of liquid metal fast breeder reactors

1. INTRODUCTION

This program was initiated in January 1972 with the objective of developing in-core and in-vessel instrumentation for application to LMFBRs. The program is presently divided into six major tasks.

Task A — In-Vessel High-Temperature Neutron Detectors and Cable Assemblies

Task B — Acoustic Monitoring Systems for Application to the Reactor Core and Vessel

Task C — Reactor Core Exit Instrumentation

Task D — Failed Fuel Detection and Location Systems

Task E — On-Line Reactivity Computer System

Task F — Plant Protection System

Primary emphasis is on overall system development, making maximum use of base technology information available from the national laboratories.

2. SUMMARY

2.1 GENERAL

Quality assurance engineering reviews of planning and technical documentation were performed as a continuing effort. Task and product classification was completed for Tasks A, B, C, and E.

2.2 TASK A — NEUTRON MONITORING

The objective of this task is to develop in-vessel neutron detector and cable assemblies that do not require auxiliary cooling or special shielding. Both startup (source range monitoring) and intermediate and wide range detectors are planned, with first priority given to the source range monitoring (SRM) detectors.

The following significant progress was made during the quarter:

- The fabrication drawings and process and material specifications for the SRM prototype detector were conditionally approved for manufacturing at a design review meeting with AEC-RRD personnel.
- Development Model No. 2 was fabricated and the short-term tests were completed. The test results are reported in Subsection 3.1.2.
- All materials and parts for Development Model No. 3 were accumulated, and assembly was started.
- Fabrication of parts for the first two prototypes was initiated.
- The cables ordered to specifications were received and evaluation testing was started.
- A new seal vendor was contacted, and quotations were received for seals (including documentation of the manufacturing process).
- A preliminary draft of RDT Standard C17-6, "Cable, Metal Sheath Mineral Insulated, for Pulse Type Fission Neutron Detectors," was sent to the National Laboratories and to cable vendors for technical review.
- A development specification, and a quality control instruction that delineates the quality assurance provisions of the specification, were issued and sent to AEC-RRD for review and comment.

2.3 TASK B — ACOUSTIC MONITORING

The objective of this task is to develop an acoustic system for in-service surveillance of LMFBR core and in-vessel components.

Accomplishments during this reporting period were:

- Approval-in-principle (AIP) requests for acoustic "piggyback" testing in the hydraulic core mock-up (HCM) at HEDL and for irradiation testing of an acoustic probe in EBR-II were submitted to AEC-RRD. These AIPs (along with AIPs for testing in the fuel failure mock-up at ORNL and the hydraulic core mock-up auxiliary loop at HEDL, written earlier) were disapproved due to RRD's intention not to continue this task in FY-74.
- The acoustic source characterization memo describing the frequencies, amplitudes, and possible impacting sites of in-vessel components was issued.
- The development specification for the ANL support of Task B with regard to sodium immersible microphones and waveguides was issued.

- Reports summarizing the GE Corporate Research and Development Center's efforts on acoustic signal analysis and on evaluation of the acoustic test data from the heat transfer simulation loop (HTSL) at ANL were completed. These reports are published in Appendices A and B of this report.

2.4 TASK C — CORE EXIT INSTRUMENTATION

The goals of this task are to identify system functional objectives and to define and conduct the necessary development efforts that will lead to a representative core exit instrumentation (CEI) probe and signal transmission design.

The following significant progress was made during the quarter::

- An analysis of the CEI functional objectives with respect to existing sensor capabilities was completed.
- The feasibility study for the integrated instrument probe was completed.

2.5 TASK D — FAILED ELEMENT DETECTION AND LOCATION (FEDAL)

The objectives of this task are to define the requirements of a system for performing detection and location of failed fuel in LMFBR demonstration plants, summarizing available instrumentation to meet these requirements, and conducting the development work required to produce an operating FEDAL system.

No effort was expended on this task during the present quarter. During previous quarters, the state of the art of FEDAL systems at other operating and planned fast reactors was surveyed as a part of an overall fast reactor instrumentation survey. A report summarizing this survey was issued.

On-going experimental programs, both at BRD and offsite were also reviewed and revised where possible to give better support to FEDAL informational needs, including the following categories:

- Basis for FEDAL instrumentation
- Instrumentation functional requirements
- Correlation of measured signal to fuel damage
- Contamination effects

2.6 TASK E — ON-LINE REACTIVITY COMPUTER (ORC)

The goal of this task is to develop an ORC system for use in an LMFBR.

During the quarter, the ORC study report summarizing the FY-73 activities was completed, and is published in Appendix C of this report.

2.7 TASK F — PLANT PROTECTION SYSTEM

The objective of this task is to develop LMFBR plant protection system sensors that can meet on-line testability requirements. Work on this task was not initiated during FY-73.

3. TECHNICAL ACCOMPLISHMENTS

3.1 TASK A — NEUTRON MONITORING

3.1.1 SRM Prototype Detectors, Drawings, and Specifications

The SRM prototype detector design package, consisting of manufacturing drawings and material and process specifications, was submitted to AEC-RRD for review. A design review meeting with AEC-RRD personnel was subsequently held. The design package was approved conditionally (a few of the process specifications are still under development). One of the processes yet to be completed is the uranium plating of the electrodes. This process is undergoing some changes during the Development Model No. 3 fabrication stage.

3.1.2 SRM Development Model No. 2

Development Model No. 2 was fabricated this quarter and the major performance tests were completed. A long-term (1000 hours) oven test at 1050°F is underway, during which the model will be removed from the oven periodically and retested (at room temperature) for neutron sensitivity, neutron pulse width, and noise and neutron integral bias.

The integral bias curves resulting from the initial test data are shown in Figure 3-1. Two noise curves are shown: one being that run at the gamma pit facility where the gamma integral bias curve was generated, and the other at the neutron source facility where the neutron integral bias curve was generated. The two noise curves are almost identical, with a slight difference in the slope, indicating a difference in the noise spectral characteristics.

A similar set of curves is shown in Figure 3-2. These curves are the result of the theoretical analysis of Development Model No. 2, based on the techniques developed during the detector physics study and using the gain of the electronics as measured for the experimental setup. A comparison of the experimental curves with the theoretical curves shows a very close fit in each category (i.e., noise, gamma, and neutrons) and if a small increase of the gain is made for the theoretical curves, the fit is even closer. Since the measure of gain for the experimental method is subject to error within the differences noted between the experimental and theoretical data, the results from Development Model No. 2 indicate that the detector performance can be predicted within a satisfactory degree.

Figures 3-1 and 3-2 show only results for a gamma level of 2.44×10^6 R/h. Measurements were also made at 1.65×10^6 R/h in order to establish effective neutron sensitivity at the different gamma levels. Following are the different neutron sensitivities for the different gamma levels and the bases. (The sensitivity at 1×10^6 R/h was obtained by extrapolation.)

Gamma Level	Neutron Sensitivity (Experimental)	Neutron Sensitivity (Theoretical)
None	0.266 cps/nv	0.257 cps/nv
1×10^6 R/h	0.197 cps/nv	(Not Calculated)
2.44×10^6 R/h	0.154 cps/nv	0.155 cps/nv

Bases

Discriminator set for 1 cps noise plus gamma

Measured noise sensitivity = $45 \text{ pa}/\sqrt{\text{Hz}}$

Measured system gain = 1120

Neutron flux level = 1167 nv

Figure 3-1. Development Model No. 2 Integral Bias (Experimental)

Figure 3-2. Development Model No. 2 Integral Bias (Theoretical)

The neutron pulse width (time) curves are shown in Figure 3-3. These curves are from photographs of the oscilloscope display as measured after an amplification gain of approximately 70. The scope display contains the entire neutron pulse height spectrum, but only one pulse size is shown here — the most predominant pulse on the scope and the one used for reference purposes. The curves show the change in pulse width (electron collection time) as the applied voltage to the detector is increased from 150 to 600 volts, where the last three voltage levels show no appreciable change. The shape of the tail of the pulse is caused by the electronic characteristics of the cable, and connections and may vary slightly from one equipment setup to the next.

Figure 3-3. Development Model No. 2 Neutron Pulse Width

3.1.3 Development Model No. 3

All materials and parts for Development Model No. 3 have been received. Subassembly work has been started. In particular, the center electrode has been assembled and the cables have been fitted with seals. The Reng 41 springs have been finished, and deflection tests were performed.

The uranium plating process has been undergoing some modifications: first, adapting the plating process hardware to the different size electrodes of Development Model No. 3, and second, making process adjustments to account for the different plating area between Development Models Nos. 2 and 3. Development Model No. 2 had two large electrodes, whereas Development Model No. 3 has three shorter electrodes, the center one of which is plated on both sides. Also, a continuous flow cooling of the plating process has been incorporated in order to provide better temperature control of the fluid. One plating run, made after the modifications were made, yielded inconsistent results in that two surfaces were satisfactorily plated, while the other two were rejected because of insufficiently adherent coatings. The plating process and procedures are being studied to determine what changes are required to give more consistent results.

3.1.4 Cable Evaluation

The cables ordered by specification for evaluation were received. Initially these cables were baked out and fitted with glass frit seals. Cable testing is now in progress. After the first series of tests are run, the glass frit seals will be replaced with the standard design cable end seal which allows the cable to be evacuated during bake-out. The cable will be backfilled with two atmospheres of nitrogen after bake-out. The two sets of test data will then be compared to establish the improvement (if any) by the latter method of cable preparation.

The cables received had slightly lower insulation density than had been specified. This deviation from specification was authorized after an initial attempt to fabricate cables showed excessive conductor deformation when the specified insulation density was achieved. The lower insulation density increased the cable capacitance slightly, but the cable characteristic impedance met the specification.

3.1.5 Detector-Cable Seals

After some failure was experienced with the first lot of seals purchased for the program,* additional seal development activity was initiated. Continued testing of the first lot of seals and investigation of the failed seals suggested that the failures were due to a lack of process control rather than a weakness in the basic design or process. It was determined that in future procurement of seals, the fabrication process used would be precisely specified and documented.

A search for a seal manufacturer willing to operate under such requirements was begun. One seal manufacturer was found who agreed to document the seal manufacturing process in detail and also agreed to some options on the methods of metallizing the ceramic. A request for quotation (RFQ) was sent to this vendor, and bids have been received and are presently being reviewed.

Three seals of the first lot were placed in a long-term oven test at 565°C. So far, these seals have experienced 2200 hours at 565°C and seven thermal cycles with no failures.

During the previous quarter, the three seals that failed the accelerated oven tests at 650°C, and a new seal that had never been tested, were sectioned, polished, and etched for a metallographic examination (see Figure 3-4). The general conclusion of the examination conducted during the present quarter was that the seals failed at the boundary of the copper braze and the metallizing layer (Figure 3-5). The metallizing seemed to be much thicker in some seals than in others (Figure 3-6), leading to the conclusion that this particular part of the process was not uniformly controlled. It was not definitely established that this was the cause of the failures, however. Since some of the seals completed 1654 hours at 650°C without failure, it was concluded that the basic design or process was not at fault.

3.1.6 General

A development specification was issued and sent to AEC-RRD for approval and comments. This specification defines the necessary requirements of documentation and approvals for the SRM development phases of development models, prototypes, and qualification units. A quality assurance instruction called for as a part of the specification was prepared, issued, and sent to AEC-RRD.

RDT Standard C17-6, "Cable, Metal Sheath Mineral Insulated, for Pulse Type Fission Neutron Detectors" was drafted and sent out to appropriate national laboratories and cable vendors for technical review.

Fabrication of parts for the first two prototypes was initiated.

A test plan for qualification testing of the prototype units was written and submitted to AEC-RRD. This plan incorporates recent redirection proposed by AEC-RRD for testing of the prototype detector in EBR-II. The plan now provides that Argonne National Laboratory will undertake responsibility for conducting all testing in EBR-II, and that the test at EBR-II will not incorporate provisions for retraction or insertion of the detector in the test well. To provide for insertion and retraction tests, facilities at the GE-San Jose site were evaluated and found to be satisfactory. Tests, using these facilities, are factored into the SRM prototype test plan.

*P.A. 65 Fifth Quarterly Progress Report, GEAP-13825-5.

Figure 3-4. A Typical Seal — Sectioned, Ground, Polished, and Etched

(500X, AS POLISHED)

Figure 3-5. Braze Region of Ceramic-Metal Seal after Test. Crack is Located Between Metallized Layer and Copper Braze.

Figure 3-6. Cross Section of Ceramic-To-Metal Seal

Without the ability to retract the detector in the EBR-II test well, the in-reactor tests cannot include long-term operation in a neutron and gamma environment at the high polarizing voltage levels required for counting operation. To provide for this test requirement, the gamma pit at the GE-Vallecitos site is being considered. The detector would be exposed to a high gamma field while operating in the counting range from a neutron source and being heated to 565°C with mineral insulated electrical heaters.

3.2 ACOUSTIC MONITORING

3.2.1 Overall Task Planning and Integration

AEC-RRD directed GE-BRD to limit further effort on Task B, Acoustic Monitoring, to documentation of activities to date.

3.2.2 Acoustic Source Characterization of Core Hardware and Other In-Vessel Components

The memo reporting acoustic source characterization was issued. This memo defines the displacement modes, vibrational frequencies, and impact points of in-vessel components. Also, it contains a discussion of the predicted character of the sodium boiling signal. The selection of transducers, transducer sites, and signal conditioning techniques requires this information.

3.2.3 Acoustic Transducer and Waveguide Development

Argonne National Laboratory (ANL) is developing sodium-immersible transducers and waveguides in support of Task B. GE-BRD issued the development specifications for these items.

3.2.4 Signal Characterization Tests

3.2.4.1 Testing Plan

To define the transducers and signal conditioning means for detecting sodium boiling and impacting in-vessel (including in-core), experimental characterization of both the signal and the prototypic background noise is required. Tests funded by other programs were to be utilized for obtaining acoustic data by adding acoustic sensors to the experimental setups. AIP requests for acoustic monitoring of several tests in these other programs were issued.

Approval was granted for participation in acoustic monitoring of the heat transfer simulation loop (HTSL) at ANL. The other AIPs (including acoustic monitoring of the hydraulic core mock-up (HCM) and auxiliary loop at HEDL, the failed fuel mock-up (FFM) at ORNL, and a planned irradiation test of an acoustic probe in EBR-II) were disapproved due to AEC-RRD's intention not to continue this task in FY-74.

3.2.4.2 Simulated Fuel Assembly Voiding Tests in the HTSL.

To evaluate the signal-to-noise ratio of sodium voiding signal detection in an LMFBR noise background, the spectrum of the signal obtained from accelerometers on the HTSL waveguide was compared with the background spectrum obtained from accelerometers on waveguides in EBR-II. In the latter case, structural members were used as waveguides.* The spectral distribution of the acceleration signals from EBR-II and HTSL varies significantly. The dimensional and configurational differences would contribute to this, of course.

The time domain signal showing the onset of channel voiding, obtained with the HTSL waveguide, is shown in Figure 3-7. The peak acceleration is 0.3-0.4 g's. The frequency spectrum of the accelerometer signal during the voiding transient is shown in Figure 3-8. Over the frequency range shown, the spectrum is "white," except for resonances of the waveguide. The frequencies of these resonances, f_r , are:

$$f_r = nc/2L,$$

*Anderson, T. T., and Just, F. H., "Acoustic Boiling in Fast Breeder Reactors," ASME Congress, Philadelphia, Pennsylvania, October 13-16, 1969.

Figure 3-7. HTSL Waveguide Data, Superheat Time Trace (1V = 0.3g)

Figure 3-8. HTSL Waveguide Data, Superheat Transient Spectrum (0 dB = 100 μ bar)

where

$n = 1, 2, 3, \dots$,

$c =$ velocity of sound in steel, and

$L =$ length of waveguides

For the HTSL waveguide, the resonances are spaced at about 2.5 kHz.

In the case of the EBR-II data, the source of two of the major spectral components can be identified. A 10-kHz component was related to pump motor speed (flow). A broad band, intense, 60 to 100-kHz component observed in the signal from one EBR-II holddown position was possibly caused by cavitation (see Figure 3-9).^{*} (Cavitation can produce a significant acoustic signal while still being well below any threshold which would be of concern from a mechanical damage standpoint.) The latter component appeared only when flow was greater than 50% of full flow, and increased rapidly with increasing flow beyond that point. The peak acceleration of the EBR-II signal was about 0.1 g.^{**}

The data currently available do not permit any final conclusion to be drawn regarding the detectability of channel voiding in an LMFBR; however, these observations can be made: (1) The magnitudes of the voiding signal and background are comparable, and (2) their respective spectra are significantly different. Additional development effort, such as the signal conditioning activity and the acoustic probe testing in EBR-II, would be required in order to determine the practicality of detecting voiding acoustically.

The HTSL test results are further reported in Appendix A.

3.2.5 Acoustic Analysis

The GE Corporate Research and Development Center (CR&DC) analyzed several aspects of the acoustic monitoring problem:

- a. Stress wave amplitude in a LMFBR structure
- b. Scaling factors for impact testing
- c. Details of mechanical impact forces

A summary of the work follows, with additional details included in Appendix B.

3.2.5.1 Stress Wave Amplitude

Stress propagation in a typical LMFBR structure was analyzed to evaluate the feasibility of impact detection. The impacting of a relatively light-weight tube section, caused by failure of its weldments, was modeled. A measurable acceleration at a sensor site on the vessel wall was predicted by the analysis. Testing would be required to determine whether or not the signal would be detectable in the presence of typical LMFBR noise background.

3.2.5.2 Scaling Factors

Scaling factors were investigated to relate acoustic data from reduced scale models of typical LMFBR structure (such as the 0.285-scale hydraulic core mock-up at HEDL) to full-scale structure. The analysis revealed that if impact velocities in the reduced-scale model were made to be the same, the signal amplitude in the full-scale structure would be smaller than in the model by the scale factor, and the time response increased by the scale factor.

^{*}Anderson, T. T., and Just, F. H., "Acoustic Boiling in Fast Breeder Reactors," ASME Congress, Philadelphia, Pennsylvania, October 13-16, 1969, Figure 7.

^{**}Ibid, page 6.

Figure 3-9. Ultrasonic Spectra For the Three EBR-II Holddowns at 100% Flow (1 kHz Bandwidth)

3.2.5.3 Mechanical Impact Forces

The time dependence of the interaction of impacting member forces was investigated analytically to assist in the detection of impacts. Since structural response to excitation is largely the same, regardless of the source of excitation, monitoring the structural response is not very informative. The interaction forces were found to be complex and, in the particular case analyzed, discontinuous during a single impact. The distinctive character of these interaction forces suggests the possibility of detecting the acoustic signals associated with the impact rather than monitoring the resulting structural response.

3.3 TASK C — CORE EXIT INSTRUMENTATION (CEI)

3.3.1 Functional Objectives

The analysis of the CEI functional objectives with respect to existing sensor capabilities was completed. The objectives of this analysis were to (1) review the functional objectives and recommend modifications if warranted, (2) ascertain the compatibility of the functional objectives with existing sensor capability, and (3) indicate those areas which will require additional developmental effort or environmental testing to ensure the attaining of the particular functional objective. The results of this analysis are

- Further review of the functional objective pertaining to accuracy requirements in the low-flow regime indicates that the detection accuracy of flow change is too restrictive at the low-flow rates (> 1 ft/sec)
- Existing thermocouple (1/8 in diameter chromel-alumel) and flowmeter (eddy current) accuracy capabilities are adequate to meet the related functional objectives
- Considerable development effort would be required to allow CEI application in the plant protection system, particularly if drywells are required
- To satisfy both the temperature accuracy objectives and the objective specifying probe centerline location in-channel flow mixers would be required
- Environmental testing associated with a single fuel assembly is required to define the environmental parameter variations because of malfunctions occurring in the fuel assembly

3.3.2 Core Exit Instrumentation Evaluation

The feasibility study for the integrated instrument probe array was completed and a memo was issued. This study defined a logic system so that emphasis could be placed upon the instrumentation aspects of the problem rather than on damage propagation. The core failure modes were categorized as physical distortion, fission product, fuel dispersion, and overtemperature.

The progression of damage for each fault condition is divided into two time segments: (1) the time period between initiation of the fault and the time at which the operating limits are exceeded, and (2) the period of time between this latter point and the time at which, reactor conditions would require an extended shutdown for recovery. The length of time for each of these periods is treated as a variable. The use of variable time periods for fault progression precludes the necessity of making a detailed study of damage propagation rates. However, fuel failure propagation literature was surveyed to compare the assumed damage propagation rates to predicted rates. This approach allows for a spread of propagation rates which is expected to result from differences in the particular parameters in a given accident (e.g., angle of impingement of fission gas jet on fuel rod adjacent to leaking fuel rod).

An array of core exit instrumentation was recommended, based upon maximum protection against core accidents. The optimum array consists of a subarray of sampling probes to monitor fission product activity in the sodium stream, preferably short half-life noble fission gas activities, another subarray to monitor fuel assembly exit flow and temperature, and a third subarray to monitor acoustic emissions from the core caused by local sodium boiling, fuel assembly voiding, and fuel rod impacting.

A number of features of the optimized probe array are not available as state-of-the-art technology. These features are

- a An inert fission gas monitoring system to detect and locate failed fuel with high sensitivity
- b Core acoustic monitoring system to detect in-core impacting, subcooled local sodium boiling, and sodium voiding of fuel assemblies
- c A combined flow and temperature monitoring configuration that permits monitoring of more than one fuel assembly with a single sensor site

Testing programs are required to qualify these features. A report summarizing the combined probe study was prepared.

3.4 TASK D — FAILED ELEMENT DETECTION AND LOCATION (FEDAL)

No effort was expended on this task during the present quarter.

3.5 TASK E — ON-LINE REACTIVITY COMPUTER (ORC)

3.5.1 ORC Study Report

The study report printed in Appendix C contains a description of the work to develop a mathematical reactor model for the on-line reactivity computer. It evaluates the uncertainties associated with the prediction, on which this model is based. The model is then used to calculate the reactivity associated with representative LMFBR malfunctions. The protection provided by the ORC is related to other surveillance systems in detecting these malfunctions. The consequences of the uncertainties are discussed in terms of the probability either to not give an alarm or to give a false alarm. The need for a dynamic ORC model is discussed, and recommendations for additional work are made.

The following conclusions were drawn from this report:

- The expected uncertainty in the reactivity change from refueling to full power is large enough even after the initial operating interval that for an ORC alarm setting of ~10 cents the probability of false alarm is great enough (>50%) that the alarm would probably be ignored and the system would not provide effective protection.
- For operation within a reasonable range about full power (e.g., 75 to 110%), the false alarm probability expected after the initial operating period is very small for alarm settings down to a few cents.
- For power operation of approximately 1 week, the false alarm probability is reasonably small (~10%) for ORC alarm settings of $\geq \sim 5$ cents.
- To have a system with a small (i.e., <10%) false alarm probability and a small (i.e., <~25%) probability of failure to alarm, two conditions must be met:
 - a The ORC reactivity uncertainty must be less than 60% of the alarm setting value, and
 - b The magnitude of the reactivity anomaly must be more than ~30% greater than the alarm setting.

4. ADMINISTRATIVE

4.1 TRIPS

- a. G. R. Pflasterer and C. L. Larson attended a meeting at W-ARD on April 12, 1973 to discuss the P A. 65 work scope and results to date.
- b. K. Smith (GE-CR&DC) participated in acoustic monitoring tests of the HTSL facility at ANL-Chicago on April 17 and 18, 1973.
- c. D. Glover and M. Plummer attended meetings at HEDL and EBR-II on April 17-19, 1973 to discuss their respective programs relating to the On-Line Reactivity Computer.

4.2 VISITS

May 1 and May 2, 1973 — J. Patterson (AEC-RRD) visited Sunnyvale for a technical review of P.A. 65, and to review the design package for the Task A source range monitor prototype units.

4.3 LIST OF CONTRIBUTORS

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APPENDIX A**ACOUSTIC MONITORING OF
HEAT TRANSFER SIMULATION LOOP (HTSL) BOILING EXPERIMENT**

Acoustic data were recorded at ANL during boiling experiments on the HTSL. A Honeywell 5600 1-inch tape recorder was used to record signals from Argonne's acoustic instrumentation in the loop. The acoustic instrumentation included a sodium immersible microphone and an acoustic waveguide immersed in the sodium plenum above the test section, plus flow and burnout detectors.

Typical test conditions are as follows:

Sodium temperature	~900°F
Heater input	180 V, 180 A
Test section flow	3.6 gal/min, 18 fps at start ~1 gal/min, 5 fps at boiling inception
Plenum pressure	15 psi
Plenum level	~70%

The sensitivity of the waveguide was on the order of 3 mV/ μ bar, and the sodium immersible microphone sensitivity was 1 mV/ μ bar.

The acoustic monitoring system frequency response extended to approximately 300 kHz, however, no significant data appeared to exist above 150 kHz.

Most of the background noise was below 10 kHz. The sodium-immersible microphone detected peaks at 800, 1200, and 2000 Hz, and a group of peaks at about 6 kHz. Many pronounced peaks were apparent in the waveguide data. These corresponded to longitudinal resonance frequencies of the waveguide:

$$f_{Ln} = \frac{nCs}{2L}$$

where

$n = 1, 3, 5, \dots$ (harmonic number),

$Cs =$ Velocity of sound in steel, and

$L =$ Length of waveguide

The calculated resonances of the waveguide in 900°F sodium were multiples of 2795 Hz. The measured peaks in this background data occurred at intervals of 2750 Hz, very close to the predicted response. The levels of the spectral peaks in the waveguide and the acoustic sensor (below 20 kHz) were on the order of 10 to 50 μ bars rms.

In general, the data obtained from the two sensors exhibited good correlation, aside from the strong resonance peaks in the waveguide. To simplify the presentation of the data, the discussions here are limited to the data from the microphone. The signal from the microphone is free of the resonances and other disturbances present in the waveguide signal.

A spectral analysis was performed on background transients during the quiet (no boiling) portion of the acoustic signal. The results illustrate that although a quiet period with only background noise was being recorded, there were occasional transients in the data. Prominent peaks occurred at 28 and 35 kHz, and a broad rise occurred between 50 and 60 kHz. The levels of the background transient data were 20 dB higher than the average rms levels. Although these transients were quite infrequent, a comparison of the transients with those occurring during a boiling event revealed major differences in spectral content. In the band from 10 to 30 kHz, the average levels of the boiling transients were 20 dB higher than the background transient data and 40 dB above the average data background. The boiling signal

was also considerably larger than the background noise in the frequency range between 70 and 90 kHz. Spectra of the microphone signals are presented in Figures A-1 through A-4 for various boiling and loop conditions in the region of 20 to 40 kHz. These conditions include "quiet" background noise, boiling noise under conditions which are believed to be saturated boiling, boiling under conditions believed to be superheated, and cavitation of the sodium pump. The differences in the spectral shape of these conditions, neglecting for the moment amplitude differences, may be summarized as follows:

Background (Figure A-1) (transients)	Peak at 28 kHz Minimums at 20, 26, 32, 40 kHz Overall roll-off characteristics — flat
Cavitation (Figure A-2)	Maximum 36 - 40 kHz (broad) Minimum 20 - 24 kHz Overall roll-off, +15 dB/octave
Saturated Boiling (Figure A-3)	Broad maximum 20 - 24 kHz Minimum 36 - 40 kHz Overall roll-off, 20 dB/octave
Superheated Boiling (Figure A-4)	Broad maximum 22 - 26 kHz Minimum 40 kHz Overall roll-off -20 dB/octave

Thus, it is seen that both conditions of boiling are similar, with small variations, but are markedly different from either cavitation noise or other random transient events included in the background.

Average spectral levels in this frequency range are essentially what would be expected. Background transients are $3 \mu\text{bar}$, cavitation and saturated boiling $30 \mu\text{bar}$, and superheated boiling $150 \mu\text{bar}$. Randomly averaged background noise is less than $1 \mu\text{bar}$ in this frequency band.

Figure A-1. HTSL Acoustic Background Transient Spectrum ($1 \text{ mV} \cong 1 \mu\text{bar}$)

Figure A-2. HTSL Pump Cavitation Spectrum ($1 \text{ mV} \cong 1 \mu\text{bar}$)

Figure A-3. HTSL Saturated Boiling Spectrum

Figure A-4. HTSL Superheated Boiling Spectrum

APPENDIX B

ACOUSTIC ANALYSIS

B.1 STRESS WAVE AMPLITUDE IN REACTOR STRUCTURE

To estimate the magnitude of impact signals transmitted through the reactor structure to a sensor site an impact prediction method was developed. This is essentially based on Hoppmann's work* on the prediction of transient signal due to an impact applied to a beam. Although many crude approximations are needed to apply such a beam theory to the complex reactor structure, the final result can be expressed in terms of a relatively few inputs and would therefore be useful for obtaining an order-of-magnitude estimate of the impact signal at a sensor site. A computer program is written to predict the transient acceleration at the sensor site when the initial momentum of the striking object is known. As a sample case, this was applied to the impact prediction of a sodium-supplying pipe (Figure B-1).

Hoppmann considered the transient response of a beam struck by a sphere. The beam is simply supported and the impact occurs at midspan. It is assumed that there is no plastic deformation by the impact. Once the force applied on the beam due to impact (contact force) is prescribed, the transient behavior of the beam can be easily determined. Right after the impact, the contact force is assumed to change sinusoidally until a certain time, T_L (contact time), and after that it is assumed to be zero. The amplitude of sinusoidal force is determined by energy consideration, and contact time is obtained from Hertz's impact theory between two spheres.

At sufficiently long time after the impact and in the absence of any damping, the beam maintains a natural vibration and the deflection of the beam at point x is given by

$$w(x, t) = \sum \frac{\pi m v_0 \sin \frac{n\pi}{2}}{M T_L [\lambda^2 - \omega_n^2]} \frac{\lambda}{\omega_n} \left\{ -\sin \omega_n T_L \cdot \cos \omega_n t + (1 + \cos \omega_n T_L) \cdot \sin \omega_n t \right\} \\ \times \sin \left[\frac{n\pi}{l} \left(x + \frac{l}{2} \right) \right],$$

where

$m v_0$ = Momentum of the striking sphere,

M = Mass of the beam,

λ = π/T_L , and

ω_n = Natural frequency of the beam

*Hoppman W H, Impact of a Mass on a Damped Elastically Supported Beam, J of Appl Mech, June 1948

Figure B-1. Impact Monitoring Study Configuration, Sodium Supply Pipe

It is noted that the response is proportional to the momentum of the striking object and inversely proportional to the mass of the beam. This was applied to the estimate of the impact signal of a sodium pipe. The reactor cross section is idealized as a beam with simple support at the reactor vessel flange. The pipe is replaced by a sphere having equal weight. Assuming impact occurs at a filter screen (see Figure B-1) when a pipe is vibrating at its first mode, the acceleration at a sensor site indicated on the figure would be

$$6 \times 10^{-2} \text{ in./sec}^2$$

Should the pipe break loose at the lower end and vibrate freely at its first mode, and impact occurs, the acceleration would be

$$10 \times 10^{-2} \text{ in./sec}^2$$

B.2 SCALING FACTORS FOR IMPACT TESTING

To extract meaningful experimental data from tests conducted in facilities other than an operating reactor, it is necessary to scale the parameters in some realistic way. Questions to be answered are, in addition to obvious geometrical and material similarity between scaled and full-size tests, what parameter should be kept the same so that two impact signals become similar. Again, referring to Hoppmann's work, we consider two impact configurations.

- a. We start with the beam-ball impact problem. Initially, the beam is at rest and at time $t = 0$; the ball strikes the beam at its midspan.

The general expression of the beam deflection after impact and at point x is given by Hoppman in the following way:

For $0 \leq t \leq T_L$,

$$w(x, t) = \sum \frac{\pi m v (1 + e) \sin \frac{n\pi}{2}}{2MT_L (\omega_n^2 - \lambda^2)} \frac{\lambda}{\omega_n} \left(-\sin \omega_n t + \frac{\omega_n}{\lambda} \sin \lambda t \right) \sin \frac{n\pi x}{l}.$$

For $t \geq T_L$,

$$w(x, t) = \sum \frac{\pi m v (1 + e) \sin \frac{n\pi}{2}}{2MT_L (\omega_n^2 - \lambda^2)} \frac{\lambda}{\omega_n} \left[\sin \omega_n T_L \cdot \cos \omega_n t - (1 + \cos \omega_n T_L) \times \sin \omega_n t \right] \sin \frac{n\pi x}{l},$$

where

m = Mass of a ball,

v = Striking velocity of the ball,

M = Half of the total mass of the beam,

e = Coefficient of restitution, $e \approx 1$ for $m/M \gg 1$,

ω_n = Natural frequency of the beam for the n^{th} mode,

$$\omega_n = \lambda_n / \ell^2 \sqrt{EI / \rho A}$$

λ_n : Number $(= (\pi n)^2)$,

ℓ : Length of the beam,

E : Young's modulus,

I : Area moment of inertia of beam,

ρ : Density of beam,

A : Cross-sectional area,

$$\lambda = \pi / T_L,$$

T_L = Hertz contact time,

$$T_L = 3.28 \left[\left(\frac{4}{3} \pi \rho \right)^2 \frac{(1 - \nu^2)^2}{E^2} \right]^{1/5} \frac{r}{v^{1/5}},$$

r : Radius of ball,

ν : Poisson's ratio.

Acceleration of the beam is given by twice differentiating w .

For $0 \leq t \leq T_L$,

$$\begin{aligned} \alpha(x, t) &= \sum \alpha_n(x, t) \\ &= \sum \frac{\pi^2 m v \omega_n (1 + e) \sin \frac{n\pi}{2}}{2MT_L^2 (\omega_n^2 - \lambda^2)} \left(\cos \omega_n t - \frac{\lambda}{\omega_n} \sin \lambda t \right) \sin \frac{n\pi x}{\ell}, \end{aligned} \quad (1)$$

For $t \geq T_L$,

$$\begin{aligned} \alpha(x, t) &= \sum \alpha_n(x, t) \\ &= \sum \frac{\pi^2 m v \omega_n (1 + e) \sin \frac{n\pi}{2}}{2MT_L^2 (\omega_n^2 - \lambda^2)} \left[-\sin \omega_n T_L \cdot \cos \omega_n t + (1 + \cos \omega_n T_L) \sin \omega_n t \right] \sin \frac{n\pi x}{\ell} \end{aligned} \quad (2)$$

- b. Now consider the two cases of beam-ball impact problem. The two cases are geometrically similar and the materials are the same.

We seek the condition under which the acceleration signal at the corresponding sensor site becomes similar for both cases. Denote the scaling factor by σ , where

$$\sigma = \frac{r^*}{r} = \frac{\ell^*}{\ell} = \dots$$

First, we look at Equation (2) and rewrite it in the following nondimensional form:

For $t \geq T_L$,

$$\frac{\frac{\alpha_n}{mv\omega_n}}{M(\omega_n^2 - \lambda^2)} = \frac{\pi^2 (1 + e)}{2} \sin \frac{n\pi}{2} \left[-\sin\omega_n T_L \cdot \cos\omega_n t + (1 + \cos\omega_n T_L) \sin\omega_n t \right] \times \sin \frac{n\pi x}{\ell}.$$

Thus, for the similarity to exist

$$\omega_n T_L = \omega_n^* T_L^* \quad (3)$$

under this condition, if we plot nondimensional acceleration,

$$\frac{\frac{\alpha_n}{mv\omega_n}}{M(\omega_n^2 - \lambda^2)} \quad \text{versus } \omega_n t, \text{ the signal becomes similar:}$$

In such a case,

$$\omega_n t = \omega_n^* t^*, \quad (4)$$

$$\frac{\frac{\alpha_n}{mv\omega_n}}{M(\omega_n^2 - \lambda^2)} = \frac{\frac{\alpha_n^*}{m^*v^*\omega_n^*}}{M^*(\omega_n^{*2} - \lambda^{*2})} \quad (5)$$

We examine Equations (3), (4), and (5) in more detail.

1. Equation (3)

$$\omega_n T_L = \omega_n^* T_L^* \quad (3)$$

Now

$$\omega_n = \frac{\lambda_n}{\ell^2} \sqrt{\frac{EI}{\rho A}}$$

and for a rectangular cross-section beam, $I = 1/12 bh^3$, $A = bh$.

Hence,

$$\omega_n = \lambda_n h/\ell^2 \sqrt{\frac{E}{12\rho}} ,$$

therefore,

$$\omega_n/\omega_n^* = h/h^* \ell^{*2}/\ell^2 = \sigma^2/\sigma = \sigma.$$

Similarly,

$$T_L = 3.28 \left[\left(\frac{4}{3} \pi \rho \right)^2 \frac{(1 - \nu^2)^2}{E^2} \right]^{1/5} \frac{r}{\nu^{1/5}} ,$$

therefore,

$$\frac{T_L}{T_L^*} = \frac{r}{r^*} \left(\frac{\nu^*}{\nu} \right)^{1/5} = \frac{1}{\sigma} \left(\frac{\nu^*}{\nu} \right)^{1/5} .$$

Substitution of these relations into Equation (3) yields

$$\sigma \cdot \frac{1}{\sigma} \left(\frac{\nu^*}{\nu} \right)^{1/5} = 1 ,$$

therefore,

$$\nu = \nu^* .$$

Hence, in order for two signals to be similar, *the impact velocity must be the same for both the model and full-scale tests.*

2. Time-Scaling, Equation (4)

$$\omega_n t = \omega_n^* t^* ,$$

therefore,

$$t^* = t \omega_n/\omega_n^* = \sigma t .$$

Hence, in the full-scale test, the time scale is slower than the model test by the factor σ .

3. Acceleration Scaling, Equation (5)

$$\frac{\alpha_n}{m \nu \omega_n} = \frac{\alpha_n^*}{m^* \nu^* \omega_n^*} .$$

$$\frac{M (\omega_n^2 - \lambda^2) T_L^2}{M^* (\omega_n^{*2} - \lambda^{*2}) T_L^{*2}}$$

Since $v = v^*$, $m/M = m^*/M^*$, it follows

$$\alpha_n^* = \alpha_n \frac{\omega_n^*}{\omega_n} \frac{T_L^2}{T_L^{*2}} \frac{(\omega_n^2 - \lambda^2)}{(\omega_n^{*2} - \lambda^{*2})} = \alpha_n \frac{1}{\sigma} \cdot \frac{1}{\sigma^2} \frac{\omega_n^2 - \lambda^2}{\omega_n^{*2} - \lambda^{*2}}.$$

Now

$$\begin{aligned} \omega_n^* - \lambda^{*2} &= \left(\lambda_n \frac{h^*}{\varrho^{*2}} \sqrt{\frac{E}{12\rho}} \right)^2 - \left[\frac{\pi}{3.28 \left[\left(\frac{4}{3} \pi \rho \right)^2 \frac{(1 - \nu^2)^2}{E^2} \right]^{1/5} \frac{r}{v^{*1/5}}} \right]^2 \\ &= \frac{1}{\sigma^2} \left\{ \left(\lambda_n \frac{h}{\varrho^2} \sqrt{\frac{E}{12\rho}} \right)^2 - \left[\frac{\pi}{3.28 \left[\left(\frac{4}{3} \pi \rho \right)^2 \frac{(1 - \nu^2)^2}{E^2} \right]^{1/5} \frac{r}{v^{1/5}}} \right]^2 \right\} \\ &= \frac{1}{\sigma^2} (\omega_n^2 - \lambda^2). \end{aligned}$$

Thus,

$$\alpha_n^* = 1/\sigma \alpha_n,$$

therefore,

$$\alpha = 1/\sigma \alpha.$$

Hence, in the full-scale test, the acceleration is smaller than the model test by the factor of $1/\sigma$.

- c. So far, the consideration is restricted to $t \geq T_L$. For $t \geq T_L$, the acceleration is given by Equation (2) and the foregoing similarity argument applies even to this case, provided $\lambda/\omega_n \sin \lambda t$ is similar. This can be indeed shown to be so by rewriting:

$$\frac{\lambda}{\omega_n} \sin \lambda t = \frac{\pi}{(\omega_n T_L)} \sin \left[\frac{\pi}{(\omega_n T_L)} \omega_n t \right],$$

and noting $\omega_n T_L$ is the same from Equation (3). Thus, for any time, t , the two signals become similar in the nondimensional plot.

- d. Summarizing, for the model and full-scale tests to be similar, the impact velocity must be the same and the two signals in the dimensional plot must look as follows:

B.3 DETAILS OF MECHANICAL IMPACT FORCES

An interesting example which can be analyzed by a generalized impact computer program (GE-CR&DC program, REACTOR I) is that of a ball bouncing on a simply supported beam. For simplicity of presentation, we take the ball as striking the beam at its mid-length.

We model the beam by including its motion in the 1, 3, 5, . . . natural modes. (The even modes would be included if the ball struck off-center.)

We model the ball as a mass to which the force of gravity is applied.

We model the springiness at the impact region by including a "stop" between ball and beam, set to close at impact.

In Figure B-2, the generalized coordinates for a simply supported beam in its lowest frequency mode are described:

Mode Shape:	$y = \sin (\pi x / \ell)$
Frequency:	$(2.85 / 2\pi) (h / \ell^2) \sqrt{E q / \gamma}$
General Mass:	$0.5 M = 0.5 (b h \ell \gamma / g)$
General Spring:	$4.06 b E (h / \ell)^3$
Center Displacement in Mode:	1.0

For the 3, 5, 7 . . . m th mode, the values would be:

Mode Shape:	$y = \sin (m \pi x / \ell)$
Frequency:	$(2.85 / 2\pi) (h m^2 / \ell^2) \sqrt{E q / \gamma}$
General Mass:	$0.5 M = 0.5 (b h \ell \gamma / g)$
General Spring:	$4.06 m^4 b E (h / \ell)^3$
Center Displacement in Mode:	$(-1)^{\frac{m-1}{2}}$

For this example, the beam is 80 inches long, 1.32 inches wide, and 0.316 inch thick (Figure B-2). Only the first four symmetric modes are included. The material is steel, with $E = 30,000,000 \text{ lb/in.}^2$ and $\gamma = 0.3 \text{ lb/in.}^3$. Thus,

$$b = 1.32 \text{ in.},$$

$$h = 0.316 \text{ in.},$$

Figure B-2. Weight of 10 Pounds Dropping 0.25 Inch onto a Flexible Steel Beam Simply Supported at the Ends

$$\ell = 80.0 \text{ in.},$$

$$g = 386.0 \text{ in./sec}^2, \text{ and}$$

$$\gamma = 0.3 \text{ lb/in.}^3.$$

$$\text{Beam Mass} = M = 0.0259 \text{ lb-sec}^2/\text{in.}$$

$$\text{Generalized Mass, all modes} = 0.01295 \text{ lb-sec}^2/\text{in.}$$

$$K_1 = (\text{Generalized Spring, } m = 1) = 9.9 \text{ lb/in.}$$

$$K_3 = 81 \times 9.9 = 802 \text{ lb/in.},$$

$$K_5 = 625 \times 9.9 = 6180 \text{ lb/in.}, \text{ and}$$

$$K_7 = 2401 \times 9.9 = 23750 \text{ lb/in.}$$

$$D_1 = (\text{Center Deflection, } m = 1) = 1.0.$$

$$D_3 = -1.0,$$

$$D_5 = 1.0, \text{ and}$$

$$D_7 = -1.0$$

The ball has a weight of 10 lb. (Mass = 0.0259 lb-sec²/in.) and the stiffness of the local contact between ball and beam is 500 lb/in. The contact with a stop has an initial clearance equal to the 0.25-inch height of the ball above the beam and a spring rate of 500 lb/in. The pull of gravity on the ball is modeled by applying to it a constant force of 10 lb.

A diagram of the problem is shown in Figure B-2. Since the frequency when $m = 7$ is

$$\left(\frac{2.85}{2\pi} \right) \left(\frac{0.316 \times 7}{80^2} \right)^2 \sqrt{\frac{30,000,000 \times 386}{0.3}} = 216 \text{ cps},$$

and good accuracy requires a time step of about 1/20 of the shortest period, it was taken as 0.00025 sec. The printout time-step was taken as 0.001 sec since it was desired to see the force detail. For deflections only, a value of 0.005 sec for the time-step would have been adequate. The results are shown in Figures B-3 and B-4.

It is interesting to note that the ball first contacts the beam after about 0.036 sec when it has fallen the 0.25-inch initial clearance between itself and the beam. The contact of the ball with the beam is not steady. There is separation at $t = 0.06$ sec, recontact at $t = 0.085$ sec, separation again at $t = 0.112$ sec, recontact at $t = 0.13$ sec, etc. The deflection of the ball (Figure B-4) and the deflection in the first mode differ by about 0.25 inch as they should. The higher modes have small deflections. Their contributions to the force are significant, however, and cause the higher frequency components in the force curve (Figure B-3).

Figure B-3. Contact Force Between Ball and Beam

Figure B-4. Displacement and Deflection Mode Amplitude versus Time

APPENDIX C
ENGINEERING EVALUATION OF
ORC FUNCTIONAL REQUIREMENTS AND CALCULATED UNCERTAINTIES
(H. S. Bailey, S. L. Derby, M. C. Plummer)

C.1 INTRODUCTION

The objective of the On-line Reactivity Computer (ORC) is to detect anomalous reactivity events and allow corrective action to be taken before serious damage occurs or propagates. In order to evaluate the hardware and development program requirements for an anomalous reactivity detection system for the LMFBR Demonstration Plant and the degree of protection offered by such a system, it is necessary to develop a conceptual design based on certain simplifying assumptions that hopefully do not alter the conclusions obtained. For the present study, the following assumptions have been made.

A GE LMFBR Demonstration Plant design is used as a representative design.

Only the equilibrium operating cycle is considered, although the initial and transition core loadings may present special problems.

Only static operation is considered in the model development and numerical evaluation, i.e., changes between equilibrium states after transients have died out. Errors expected during transient cases are discussed in Section C.6 and will place special requirements on the system.

The following sections summarize the ORC model that has been developed using these assumptions and the related uncertainty analyses. Wherever possible, special note is made of specific technical assumptions or approximations and areas where significant future effort is required. The conclusions drawn from this study are intended to be used in developing an optimum strategy for use of the ORC and as guidelines for future development work.

C.2 REACTIVITY MODEL

C.2.1 Schematic Arrangement

The conceptual arrangement of the on-line reactivity computer is shown in Figure 2-1. The calculated, or predicted, reactivity is broken into three major categories:

Feedback reactivity refers to reactivity changes from a specified reference state caused by thermal expansion and Doppler effect related to changes in power level or coolant flow and temperature.

Burnup reactivity refers to the loss-of-reactivity caused by the net depletion of fissile material in the core and the buildup of fission products. This is related to the accumulated reactor exposure. Special items included in this category are the reactivity addition resulting from refueling and the reactivity loss caused by the swelling of the fuel and core structure.

Control system worth refers to the reactivity change caused by movement of the nuclear control system from a specified state. Burnup of the absorber material is included in this item.

These reactivities are compared with the actual system reactivity defined below.

Measured reactivity refers to the system reactivity measured from the time variation of the neutron flux using an inverse kinetics calculation. This portion of the system concept is included for completeness only at this point, since for the steady state cases considered at present, the measured reactivity will be zero.

C.2.2 Calculational Model

The calculational model described below is intended to provide a method of predicting the changes in reactivity experienced during startup, changes in flow or power level, and normal power operation. A reference state of the reactor is assumed at which the reactor parameters are well known and the predicted and measured reactivity balance. At this point in the ORC development, the reference state (e.g., at full power beginning-of-cycle or cold critical) has not been established nor has the frequency with which the ORC

FIGURE C-1

SCHEMATIC ARRANGEMENT FOR ORC

must be rebalanced and the reference state updated been established. However, for this model development, it has been assumed that the reference state is at full power operation at the beginning of an equilibrium operating cycle. The equation which the ORC solves is then simply

$$\rho_{\text{anom.}} = \rho_{\text{meas.}} + \Delta k_{\text{f.b.}} + \Delta k_{\text{burn}} + \Delta k_{\text{control}}$$

where

$\rho_{\text{anom.}}$ is the anomalous reactivity,

$\rho_{\text{meas.}}$ is the measured system reactivity,

$\Delta k_{\text{f.b.}}$ is the predicted reactivity gain from feedback associated with a specified change from the reference state,

Δk_{burn} is the predicted reactivity gain from burnup (note that this value is negative except when fuel is replaced during refueling),

$\Delta k_{\text{control}}$ is the predicted reactivity gain caused by the movement of the control rods in going from the reference state to another, specified equilibrium state.

The value of $\rho_{\text{anom.}}$ should be zero for normal operation, that is the feedback and burnup reactivity should be just offset by the control system worth change. A non-zero value of $\rho_{\text{anom.}}$ indicates either that an unexpected event, e.g., loss of fuel, has occurred or that the model has not correctly predicted one or more of the reactivity components. The primary objective in the model development is to reduce the likelihood of the latter occurrence to an acceptable value.

Measured Reactivity

The measured system reactivity will be based on an inverse kinetics calculation similar to that developed for FERMI.⁽¹⁾

(1). E.R. Volk, et al, "Design and Capabilities of the Malfunction Detection Analyzer Installed on the Enrico Fermi Fast Breeder Reactor", APDA-LA-3, July 1969.

Feedback

The feedback reactivity is calculated by combining the individual contributions for a specified change in reactor conditions. For this analysis the reactivity change is produced by geometric and material density changes from a reference condition, assumed to be full power, full flow. The nuclear coefficients relate these changes directly to reactivity as shown below:

$$\frac{dk}{k} = \left(R \frac{\partial k}{\partial R} \right) \frac{dR}{R} + \left(H \frac{\partial k}{\partial H} \right) \frac{dH}{H} + \sum_{i=1}^M \left(n_i \frac{\partial k}{\partial n_i} \right) \frac{dn_i}{n_i} + \left(T_f \frac{dk}{dT_f} \right) \frac{dT_f}{T_f}$$

where $\frac{dk}{k}$ = total reactivity change

with nuclear coefficients

$R \frac{\partial k}{\partial R}$ = reactivity coefficient for radius change only

$H \frac{\partial k}{\partial H}$ = reactivity coefficient for height change only

$n_i \frac{\partial k}{\partial n_i}$ = reactivity coefficient for material i density change only

$T_f \frac{dk}{dT_f}$ = Doppler coefficient for changes in fuel temperature, T_f

n_i = density for material i with,

$i = 1$ fuel

$i = 2$ steel

$i = 3$ control

$i = 4$ sodium

and geometric changes

$$\frac{dR}{R} = \text{fractional core radius changes}$$

$$\frac{dH}{H} = \text{fractional core height changes}$$

$$\frac{dn_i}{n_i} = \text{fractional material } i \text{ density change due to all effects.}$$

It has been assumed that the reactivity effect caused by changes in material densities, which are effected by core component distortions in addition to usual thermal expansion, can be adequately related to a single parameter, n_i , representing the density of material i . Because of the possibility of complex bowing of fuel assemblies, this assumption may prove to be an unsatisfactory oversimplification. Section C.7 discusses the additional development required to obtain a satisfactory model for the core thermal-mechanical feedback. The simplifying assumptions incorporated in the model used for this evaluation are not felt to effect the conclusions reached since accurate determination of the absolute value of the parameters is not the purpose.

The core radius change is specifically attributed to the steel structure dimensional changes. The fuel radial distribution within the core is assumed to follow the steel. The core height change is specifically attributed to the fuel height change. The material density, n_i , is defined axially by the change in the material temperature itself and radially by the change in the steel structure. The sodium coolant, however, is not geometrically constrained and assumed to be only temperature dependent. The change in density for materials other than sodium is then expressed as:

$$\frac{dn_i}{n_i} = -2 \left(\frac{dR}{R} \right)_{\text{steel}} - \left(\frac{dH}{H} \right)_i$$

The geometric changes for the materials is determined by the thermal-mechanical analysis relating the material temperature to geometry. The thermal expansion is approximated as

$$\left. \frac{dR}{R} \right)_i = (A_i + B_i T_i) \frac{dT_i}{T_i}$$

$$\left. \frac{dH}{H} \right)_i = (C_i + D_i T_i) \frac{dT_i}{T_i}$$

where T_i = material temperature

and

$$\left. \begin{array}{l} A_i \\ B_i \\ C_i \\ D_i \end{array} \right\} = \text{fitting constants to describe thermal expansion for material } i$$

Axial expansion of oxide fuel is not necessarily governed by the fuel temperature and expansion coefficient but may follow the clad expansion because of adherence of the fuel to the clad and fuel restructuring. The radial distortion of the core structure, i.e., assembly channels, will be effected by in-core thermal gradient and the accumulated effects of irradiation in addition to the thermal expansion of core support structure. Accurate modeling of these effects should be included in the next improvement of the ORC model.

The material temperatures are related to the reactor power, coolant inlet temperature, and flow by thermal-hydraulic analysis:

$$\frac{dT_i}{T_i} = \alpha_i \frac{dP}{P} + \beta_i \frac{dT}{T} + \gamma_i \frac{dW}{W}$$

where

P = reactor power

T = coolant inlet temperature

W = coolant flow

and

$$\left. \begin{array}{l} \alpha_i \\ \beta_i \\ \gamma_i \end{array} \right\} = \text{fitting constants}$$

The use of these fitting coefficients makes it possible to account for non-linear effects such as changes in heat transfer parameters. As noted in Section C.3.1, the detailed model used to relate the core geometry changes to the reactor operating parameters depends on analysis of core component mechanical interaction under the influence of thermal gradients, radiation induced swelling and creep, and the core restraint system. It is assumed here that fitting constants can be chosen to accurately match the reactivity effects from detailed analysis and/or measurements.

Burnup

The burnup reactivity may be expressed as

$$\Delta k_{\text{burn}} = \int_{T_R}^T \left[a P(t) + b P^2(t) \right] dt + \Delta k_{\text{Pu24I}}(T - T_R) + \Delta k_{\text{refuel}}$$

where

t = time variable

T_R = time at which reference state existed

T = time at which Δk_{burn} is being evaluated

$P(t)$ = reactor power at t

a = linear coefficient of burnup reactivity loss

b = second order coefficient of burnup reactivity loss

$\Delta k_{\text{Pu24I}}(T_1 - T_2)$ = reactivity loss due to Pu-24I decay between time T_1 and T_2

Δk_{refuel} = the reactivity addition caused by replacement of fuel.

The burnup reactivity loss includes the effect of fuel axial growth and radial movement of core components due to radiation induced swelling and creep. The inclusion of the second order burnup term allows correct modeling of the non-linear reactivity loss with burnup and of the non-linear burnup effects caused by the control system withdrawal. The reactivity addition caused by refueling, Δk_{refuel} , must be evaluated at the reference state rather than at refueling conditions to have the equation consistent.

Control System Worth

The control system reactivity of interest is the change in the reactivity worth of the inserted neutron absorber elements corresponding to a specified change in rod position from the reference state, plus the change in worth caused by burnup of the absorber. Because the reference state will undoubtedly be a critical state and because the ORC is not intended to function for subcritical states (this is the proper domain of the subcriticality monitoring system) the effect of the safety control rods, which are withdrawn before the reactor is critical, need not be considered.

The prediction of the control system worth is considerably complicated by the interactive effects among the control assemblies. Results from the ZPPR-3 program⁽²⁾ show that single assembly worths can be changed by $\pm 15\%$ (i.e., $\pm 30\%$ for Demo Plant control assemblies worth $\sim 2\%$) with reasonable insertion patterns and by a factor of 2 for extreme insertion patterns. Preliminary results from the ZPPR-3 analysis program show that the worth of typical control system insertion patterns can be predicted within a few percent of the measured values. Additionally, the calculations correctly predict change in worth of specific assemblies caused by the insertion of other assemblies.

While this agreement between predicted and measured control worth in ZPPR-3 is very encouraging, it must be noted that the necessary calculations are time-consuming, expensive and require a very large computer, e.g., CDC 7600. For realistic control patterns with partial axial insertion, three-dimensional diffusion theory calculations with at least six neutron energy groups are required and must be corrected by additional transport theory calculations. These calculations are clearly beyond the capability of an on-line data processing system and therefore a simplified, approximate method is required that is within the data handling system capabilities and that will predict the control insertion worth, based on rigorous prior calculations and measurements, with the accuracy required for ORC. Development of such a semi-empirical model has begun under the AEC funded Critical Experiments and Cooperative Nuclear Data task, and is planned to continue in GFY-74.

(2). "Critical Experiments and Cooperative Evaluations Program, Fifth Quarterly Report, October-December 1972", GEAP-13771-5, December 1972.

The method being developed is based on use of rod-pair influence functions and will be improved as necessary to provide the accuracy required for specific applications. The accuracy requirements for ORC application are being considered in developing this approximate control worth model. For the purposes of this report the control system worth will simply be indicated by the function

$$\Delta k_{\text{control}} = f\left(\left\{Z_i^R\right\}_{i=1,M}, \left\{Z_i\right\}_{i=1,M}\right) + \sum_{i=1}^M \int_{Z=0}^{Z=Z_i(T)} \int_{T_R}^T \frac{\partial \rho}{\partial N_{B-10,i}(Z)} \frac{\partial N_{B-10,i}(Z)}{\partial t} dt$$

where

$\left\{Z_i^R\right\}_{i=1,M}$ is the set of numbers indicating the insertion position of each of the M control rods in the reference conditions

$\left\{Z_i\right\}_{i=1,M}$ is the set of numbers indicating the insertion position of each of the M control rods for the present condition

$f\left(\left\{Z_i^R\right\}_{i=1,M}, \left\{Z_i\right\}_{i=1,M}\right)$ is the change in control system worth caused by movement from Z^R to Z

ρ is the reactivity

$N_{B-10,i}(Z)$ is the atom density of B-10 at height Z for control rod i

Thus, the first term gives the change in worth caused by control movement, although the functional form is not yet specified, and the second term gives the change in worth caused by boron-10 burnup.

C.3 UNCERTAINTIES IN REACTIVITY CALCULATIONS

In order to evaluate the degree of protection afforded by the ORC system, estimates have been made of the uncertainty in predicting core reactivity changes that stem from model and data uncertainties. These uncertainties are separate from the possible errors caused by instrumentation uncertainties. The model and data uncertainties have been estimated for three points in an LMFBR plant project: 1) current status, 2) status expected at the conclusion of initial testing and power ascension, and 3) status expected at the end of the initial power operating interval. Although there is considerable subjective judgement incorporated in these uncertainty estimates, it is felt that they contribute to a valid assessment of the range of protection afforded by an ORC system and provide guidance for development efforts.

C.3.1 Feedback Reactivity Uncertainty

Errors in predicting the feedback reactivity stem from three sources:

1. Prediction of the temperature distribution and the mechanical behavior of the core components under power and flow changes,
2. Calculation of the nuclear effect, i.e., reactivity change, caused by changes in position and density of core components, and
3. Calculation of the Doppler feedback reactivity.

The uncertainty in thermal and mechanical behavior of the core components is estimated to cause ~20% uncertainty (1 σ value) in the reactivity effect. Because the thermal-mechanical effects contribute ~30% of the power coefficient, this uncertainty causes a 6% (i.e., 20% of 30%) uncertainty in the power coefficient at present as shown in Table C-1. This is expected to decrease to ~4% uncertainty after initial operation and measurement of component distortions. The variation in oxide fuel axial expansion coefficient, i.e., axial expansion determined by fuel centerline temperature initially and possibly by clad expansion later, which has been observed in RAPSODIE and

TABLE C-1

ESTIMATED FEEDBACK REACTIVITY UNCERTAINTIES

	Contribution ⁽¹⁾ to Error in Power Coefficient ⁽²⁾		
	<u>Now</u>	<u>After Startup Tests</u>	<u>After Initial Operating Cycle</u>
Thermal and Mechanical Behavior	6%	4%	2%
Nuclear Analysis of Material Movement	1%	<1%	<1%
Doppler	11%	7%	4%
Statistical Sum	<u>13%</u>	<u>8%</u>	<u>5%</u>

(1) Material motion and Na density change contribute 30% of the calculated power coefficient with Doppler feedback contributing the remaining 70%.

(2) Although the power coefficient is specifically considered here, the errors in predicting the reactivity effect of flow changes, inlet temperature changes, and combinations of these will be of a similar magnitude.

is discussed further in "Reactivity Surveillance Procedures for the Fast Flux Test Facility Reactor", ⁽³⁾ and the complicated thermally induced bowing interaction of core component are the major reasons for the remaining uncertainty after initial operation. The assignment of errors to specific sources after the initial operating cycle is somewhat arbitrary. The statistical sum, ~5% error, is based on discussions between H.S. Bailey of General Electric and CEA personnel on experience in predicting and analyzing the power coefficient of RAPSODIE. CEA stated that they can calculate the RAPSODIE power coefficient with 5% accuracy quite easily. The 5% remaining uncertainty also seems reasonable in light of the EBR-II expected uncertainty (<10% error for the full range from startup to full power) and the FERMI experience of 1-2% uncertainty. The ability to predict the reactivity effect from a specified change of position and density of core components is estimated to be ~5% now. This relatively small source of error will be reduced somewhat by nuclear data improvements and continued critical experiment measurements. The present Doppler uncertainty of 15% is based on the SEFOR results ⁽⁴⁾ and consideration of the complicating effects of the control rods and the higher fuel temperature. As seen in Table C-1, it is expected that the Doppler uncertainty will be reduced to 10%, primarily because of critical experiment measurements of the control rod influence which are being conducted under GE direction in the ZPPR-3 program.

3.2 Uncertainty in Prediction of Burnup Reactivity

The uncertainty in burnup reactivity is mainly caused by uncertainty in nuclear cross section data and flux spectra predictions which result in uncertainty in the change of fuel isotope compositions with exposure and by the uncertainty in fission product reactivity effects. Additional sources of error in the burnup reactivity are associated with the axial growth of the fuel column and the irradiation induced distortion of the core components. Table C-2 indicates the estimated values for the characteristic points in project. These values do not include the uncertainty in determining the absolute power level from heat balance data, which may be as large as 5%. For power operation, the change in power level, from flux monitors, is expected to be accurate to within 1-2% if the effect of control rods is properly included. The Δk values are based on calculations for a GE Demo Plant

(3). W/FFTF-732145, March, 1973, compiled by J.W. Upton, Jr.

(4). L. Caldarola, et al, "SEFOR Experimental Results and Application to LMFBR's", paper presented at the International Conference on Engineering of Fast Reactors for Safe and Reliable Operation, Karlsruhe, Germany, October 9-13, 1972.

TABLE C-2

BURNUP REACTIVITY UNCERTAINTIES
(1 σ Values)

	<u>Present 1973</u>	<u>Start of Initial Operation</u>	<u>After Initial Operating Cycle</u>
Fractional Uncertainty in Burnup Δk	20%	15%	3%
Uncertainty in Δk^* for Full Power Operation of:			
One Week	0.10\$	0.07\$	< .01\$
One Month	0.42\$	0.31\$	0.06\$
One Year	5.0\$	3.75\$	0.75\$

*Based on burnup reactivity loss of 25\$ for one year operating cycle.

design equilibrium operating cycle, which has a total burnup reactivity of 25\$ each cycle. The value for the W-ARD Demo Plant target operation is ~20% less than this value, i.e., the burnup reactivity loss is expected to be ~20\$. As shown in Table C-2, the burnup reactivity uncertainty will be reduced only a small amount by development work prior to power operation and will drop significantly only after the initial operating period. This uncertainty may be reduced more before power operation if burnup measurements being conducted by the French CEA are successful.⁽⁵⁾

C.3.3 Uncertainty in Control System Reactivity

Evaluation of the uncertainty in predicting control system reactivity worths has been mainly based on the measurement and analysis of control rod worths in the ZPPR-3 critical experiment program.⁽⁶⁾ Table C-3 illustrates a typical comparison of measured and calculated rod pattern worths from this program. It is seen that the calculation to experiment (C/E) ratio is generally within 7% of unity and that application of a bias factor correction would reduce the variation of C/E from unity to $\pm \sim 2\%$ for a wide range of insertion patterns. This error will be increased somewhat by using an approximate model in ORC rather than the direct calculation of worth. It is hoped that this error will be $\leq 3\%$. In an operating LMFBR there will be additional uncertainties caused by the control absorber burnup and the effect of refueling patterns on control worth. From these considerations the estimates shown in Table C-4 are derived.

C.3.4 Combined Uncertainty

The uncertainty estimates discussed in the preceding sections have been combined statistically, assuming no correlation, to obtain the estimated uncertainty in the predicted reactivity change resulting from a change from the reference state. These values are given in Table C-5. To relate these values to postulated malfunctions, the reactivity changes accompanying specific operation changes are considered. Table C-6 gives the expected reactivity change, to be compensated by control rod movement, and estimated uncertainty for several typical cases.

(5). Discussed in memo from H.S. Bailey, "Trip Report on Power Reactor Physics Meeting, Julich, Germany, and Meetings with Fast Reactor Nuclear Design Group at Caderache, France, and Winfrith, England", May 5, 1973.

(6). Documented in Quarterly Reports, GEAP-13771-1 through 13771-6.

TABLE C-3

TYPICAL MEASURED AND CALCULATED CONTROL ROD WORTHS
FROM ZPPR-3 PHASE IB⁽¹⁾

<u>Control Insertion Pattern</u> ⁽²⁾	<u>Measured Worth</u> ⁽³⁾ , \$	<u>Calculated Worth</u> ⁽⁴⁾ , \$	<u>C/E</u>
1	2.13	2.03	0.95
2	2.04	1.91	0.94
8	1.97	1.79	0.91
9	1.99	1.87	0.94
2,5	4.22	3.98	0.94
2,8	3.62	3.35	0.93
2,9	3.72	3.48	0.94
2,8,9	4.95	4.13	0.83
8,14	4.27	3.92	0.92
8,9	3.51	2.81	0.80
8,10,12,14,16,18	14.91	13.96	0.94
8,9,10,11,12,13,14, 15,16,17,18,19	26.36	26.44	1.00
2,4,6	6.42	6.17	0.96
2,4,6,10,14,18	13.68	13.21	0.97
2,4,6,8,10,12,14,16,18	21.45	20.35	0.95
2,3,4,5,6,7,10,14,18	19.41	18.03	0.93
2,3,4,5,6,7,8,10,12, 14,16,18	27.43	26.02	0.95
All Except #1	39.32	37.95	0.97

(1) See (quarterly report) for detailed description and analysis.

(2) Numbers of inserted rods correspond to Figure C-2.

(3) Modified source multiplication measurement.

(4) Two-dimensional 6 group diffusion theory with transport corrections based on cell calculations.

FIGURE C-2. ZPPR-3 PHASE IB CORE LAYOUT

TABLE C-4

ESTIMATED UNCERTAINTY IN CONTROL SYSTEM WORTH

Percent Uncertainty in Inserted Worth
or Worth Change with Control Movement

<u>Now</u>	<u>After Initial Power Operation</u>	<u>After Initial Operating Interval</u>
10%	4%	3%

TABLE C-5

ESTIMATED COMBINED UNCERTAINTY IN REACTIVITY PREDICTION

Percent Error (1σ) of Predicted Reactivity Effect

<u>Type of Change</u>	<u>Now</u>	<u>After Initial Power Operation</u>	<u>After Initial Operating Interval</u>
Power and/or Flow Change	16%	9%	6%
Burnup at Constant Power Level	22%	16%	4%
Burnup with Swings in Power Level	26%	17%	7%

TABLE C-6

REACTIVITY CHANGE AND ESTIMATED UNCERTAINTY FOR TYPICAL OPERATIONS

	<u>Predicted Reactivity Change, \$</u>	<u>Estimated Reactivity Uncertainty, \$</u>		
		<u>Now</u>	<u>After Initial Power Operation</u>	<u>After Initial Operating Interval</u>
Refueling to Full Power ⁽¹⁾	3.00	0.50	0.35	0.20
20% Power Change from Full Power ⁽²⁾	0.34	0.08	0.05	0.01
Full Power Operation for One Week	0.48	0.12	0.09	0.03

- (1). The fractional uncertainty in the refueling to full power reactivity changes is somewhat greater than the uncertainty in the power coefficient because the relatively less certain thermal expansion plays a greater role in the refueling to full power effect than in the power coefficient.
- (2). Assumed to occur over several minutes so that thermal equilibrium is established at the final state.

C.4 CONSEQUENCES OF UNCERTAINTY

The uncertainties in the ORC calculation produce two types of possible errors that may limit the usefulness of the system:

- 1) false alarm, ORC indicates reactivity unbalance greater than alarm limit when no malfunction has in fact occurred;
- 2) failure to alarm, error in ORC calculation compensates for effect of a malfunction and alarm is not activated.

A straight-forward analysis of the probability of each of these occurrences, as a function of the alarm setting $\epsilon(\%)$ and the ORC uncertainty $\sigma(\%)$, is possible if it is assumed that the ORC uncertainty is normally distributed. Because of the number of unrelated sources or error in the ORC model, this is a reasonable first approximation.

C.4.1 Probability of False Alarm

For this consideration, it is postulated that no malfunction occurs but that the error in the ORC prediction of the reactivity accompanying changes in operating conditions or burnups is large enough to exceed the alarm limit. The probability of false alarm as a function of the ratio of alarm setting, ϵ , to the reactivity uncertainty, σ , is shown in Figure C-3. The reactivity uncertainty in % for specific operating changes are given in Table C-6. From these data, it is seen that the probability of false alarm is low, <10%, if the alarm limit is at least ~ 1.7 times the reactivity uncertainty.

C.4.2 Probability of Failure to Alarm

For this code it is postulated that a malfunction occurs during a change in operating conditions or power operation. It is possible for the error in the ORC calculation of the expected reactivity change to mask the effect of the malfunction and fail to indicate anomalous reactivity. Figure C-4 shows the probability of failure to alarm as a function of the magnitude of the malfunction reactivity. The ratio of the alarm setting to the ORC uncertainty, ϵ/σ , is shown as a parameter in these curves. This information should be used

FIGURE C-3

PROBABILITY OF FALSE ALARM

FIGURE C-4

PROBABILITY OF FAILURE TO DETECT ANOMALOUS REACTIVITY

in conjunction with the reactivity effects of postulated malfunctions discussed in Section C.5 and the false alarm probability data from Section C.4.1 to determine the optimum alarm setting and the degree of protection offered by the ORC system. For example, if a 5¢ anomalous reactivity alarm limit is considered, the values in Table C-7 indicate the probability of false alarm and failure to alarm for several typical cases. Several general observations can be made from this example:

1. The expected uncertainty in the reactivity change from refueling to full power is large enough even after the initial operating interval that for ORC alarm setting of $\sim 10\text{¢}$ the probability of false alarm is great enough ($>50\%$) that the alarm would probably be ignored and the system would not provide effective protection.
2. For operation within a reasonable range about full power (e.g., 75% to 110% of full power), the false alarm probability expected after the initial operating period is very small for alarm settings down to a few cents.
3. For power operation of approximately one week, the false alarm probability is reasonably small ($\sim 10\%$) for ORC alarm settings of $\geq \sim 5\text{¢}$.
4. To have a system with a small, i.e., $<10\%$, false alarm probability and a small, i.e., $<\sim 25\%$, probability of failure to alarm two conditions must be met:
 - the ORC reactivity uncertainty must be equal to less than 60% of the alarm setting value; and
 - the magnitude of the reactivity anomaly must be more than $\sim 30\%$ greater than the alarm setting.

The consequences of these observations are discussed in Section C.6.

TABLE C-7

ORC ALARM PROBABILITIES WITH ASSUMED 5¢ ALARM SETTING

Postulated Event	Probability of False Alarm			Probability of Failure to Alarm		
	Present Uncertainties	After Initial Operations	After Initial Operating Cycle	Present Uncertainties	After Initial Operations	After Initial Operating Cycle
Startup from Refueling to Full Power, No Anomalous Reactivity	85%	80%	77%			
10% Power Change from Full Power, No Anoma- lous Reactivity	20%	8%	~0			
One Week Full Power Operation, No Anoma- lous Reactivity	70%	60%	10%			
Startup from Refueling to Full Power, 5¢ Anomalous Reactivity				~8%	~7%	~5%
10% Change from Full Power, 5¢ Anomalous Reactivity				10%	50%	50%
One Week Full Power Operation, 5¢ Anoma- lous Reactivity				20%	40%	50%

C.5 EVALUATION OF REACTIVITY EFFECTS OF POSTULATED MALFUNCTIONS

The purpose of this section is to provide estimates of the reactivities that are associated with postulated events which the ORC system could detect. The events have been selected from a list of typical LMFBR plant potential single faults and malfunctions as the ones which generate anomalous reactivity. Each event has a postulated movement of control material, fuel, or coolant which causes changes in the system reactivity. The reactivity associated with each of these malfunctions determines the ORC's ability to detect the event. The reactivities associated with the postulated events tabulated below are based on calculations and data for a GE Demonstration Plant design. These estimates, though some are quite accurate, are only intended to be representative of the magnitude of the reactivities rather than precise calculations.

The descriptions of events which effect the ORC system are broken down into three groups depending on the likelihood with which they are expected to occur. The group described as "high frequency events" are not highly probable, but rather unlikely. The "high frequency" of incidence is only relative to the unlikely or improbable events described in the "intermediate" and "low frequency" groups.

High Frequency Group

Random Fuel Rod Failure

It is postulated that all the rods in one fuel assembly lose 100% of the fission product gases contained at end-of-cycle conditions. This would cause a reactivity increase of no more than 2β . This gas is assumed to be totally released in a period of several milliseconds to several seconds. The cover gas monitor which can respond to failure of just a few of the 217 fuel rods making up the assembly will provide indication of this malfunction. The response time of the cover gas monitor is of the order of 10 to 20 seconds.

Intermediate Frequency Group

1. Single Control Element Loss of Response

It is postulated that six control elements are being used as a bank to compensate for power level changes near to full power and for reactivity loss due to burnup. One of the six rods does not move, although the position indicator does move. With a 10% change in power level, the anomalous reactivity would cause $\sim 6\%$ anomalous reactivity. Full power operation for one week would cause $\sim 8\%$ anomalous reactivity. This malfunction will probably not be detected by any other surveillance system.

2. Loss of Response of One Control System

Loss of response of either the safety or alternate shutdown system will be detected by the subcriticality monitoring system rather than by ORC and will not be further considered. Loss of response of the complete shim system should be detected by the operator noting a lack of change in flux level and period when the system is actuated. A 5% power change or one day full power operation would lead to 10% anomalous reactivity.

3. Random Fuel Rod Failure

It is postulated that the rods in one fuel assembly at the center of the core fail and lose 1% of their fuel from core midplane area causing a reactivity loss of $\sim 3\%$. This failure is equivalent to the loss of two complete fuel rods and would be detected by the cover gas monitors and the FEDAL system.

4. Random Rod Failure in Control Element

It is postulated that one rod in the highest worth control element fails and loses 5% of the absorber from about the core midplane. The reactivity effect of this event is $\sim 1\%$. This malfunction will probably not be detected by any other surveillance system.

5. Loss of Flow to Any Fuel Assembly*

It is postulated that the flow to the highest worth fuel assembly is reduced and the fuel heats up until the expected, i.e., nominal, temperature of the fuel corresponds to 50% of the fuel reaching the liquidus point. This would cause reactivity loss of less than 1¢. The fuel assembly thermocouples are sensitive to a temperature change of 10°F and would provide indication of the malfunction prior to reaching the liquidus point.

6. Loss of Flow to Any Control Element *

It is postulated that the flow to the highest worth control assembly is reduced and the assembly heats up until the absorber temperature reaches solidus, and the absorber slumps causing a 5% compaction of a fully inserted control rod. This would cause ~12¢ reactivity decrease. The control assembly thermocouples are sensitive to a temperature change of 10°F and would provide indication of the malfunction prior to the temperature reaching solidus.

7. Injection of Gas Bubble or Entrained Gas into the Core

Introduction of a gas bubble larger than about 3 liters near the core center would cause ~3¢ reactivity addition and should be detected by the ORC system. The rapid transit time of sodium through the core, ~0.5 sec, would require a fast acting dynamic ORC model to detect a bubble during transit through the core. A gas bubble greater than ~20 liters (covering the control ~45 liters of core volume) introduced near the core center would cause ~40¢ reactivity addition and the high level flux trip setting would be reached before ORC detected the event. It should be noted that the reactivity worth of a gas bubble is very dependent on its location in the core so that a bubble would cause reactivity to decrease, rise, decrease, and then return to the original value as it enters the bottom of the core, passes through the center portion of the core, and then is swept out of the core. It may be postulated that entrained gases may be present in the core in

*The complete accident sequence, including sodium boiling, is not covered in this treatment.

in a near steady state condition. For this case, it will be assumed that 1% of the core sodium volume is composed of small gas bubbles. This would cause a reactivity change of $\sim 3\text{c}$. The assembly flowmeters are able to distinguish 1 cc per second of gas in the sodium flow, and would detect this malfunction.

8. Failure of core restraint system resulting in either radial or axial motion of core components with the resulting reactivity within the capability of control system.

It is postulated that the failure occurs rapidly compared to the burnup reactivity loss ($\sim 0.5\%$ per week), and is limited to 0.05% as required in the GE Demo Plant Core SDD. This corresponds to an average radial movement of about 5 mils. It should be noted that if there is a slow failure or very incoherent series of failures, the reactivity effect may be indistinguishable from burnup or swelling effects. This malfunction will probably not be detected by any other surveillance system.

Low Frequency Group

1. Expulsion of Control Material from a Single Control Element

It is postulated that all the rods in the highest worth control element fail and lose 5% of the B_4C from about the core midplane causing a reactivity increase of $\sim 7\text{c}$. This malfunction will not be detected by other surveillance system.

2. Injection of Large Gas Bubble into the Core

As noted above, a gas bubble that would induce a significant reactivity ($\sim 40\text{c}$) would be detected by the neutron level monitors. The flowmeters and possibly the acoustical monitors would also detect this malfunction.

3. Failure of the Core Restraint System Resulting in Gross Core Movement

Based on the core restraint design requirements, it is postulated that the reactivity addition from failures is limited to 0.80% . There will be many indications resulting from a reactivity addition of this size.

C.6 ERRORS IN DYNAMIC CONDITIONS

The purpose of this section is to quantify the errors that can result when only a static model is used to predict the instantaneous reactivity requirement. To do this, we examine what rates of change of power level and temperatures exist during load following power setback, and startup/shutdown transients. At this point, a specific dynamic model has not been developed. The following analyses have been performed to evaluate the desirability of including dynamic effects in the ORC model.

Load Following Transients

The most rapid load change which the reactor is required to follow is specified as a step change of $\pm 10\%$ of full power. Practically, however, step changes cannot be accomplished at rates greater than 1% per second. Larger power changes (60 to 70% of full power) require lower rates of change. Typically, 3% per minute are quoted as acceptable rates of change above 40% of full power. Below 40%, the temperature rate of change is limiting and nominal limits of 50 to 100°F/hr are generally stated (see discussion below).

The errors that a static model would make in following a 10% step change in power are related to the time constant of the fuel temperature. The fuel time constant is strongly dependent on the gap conductance, but is in the range of 2 to 5 seconds. Assuming that the control drive was sufficiently fast to cause a 10% flux change in one second, the fuel temperature would require 4 to 10 seconds to achieve 90% of the change to the new fuel temperature. At full power, a 10% change in power requires approximately 25¢ of reactivity, most of which is Doppler feedback related directly to fuel temperature. At the instant the flux level change is complete, the fuel temperature remains at nearly the same temperature as in the start of the transient. The reactivity error of a static model is about 15 to 20¢ at this instant. If 10 seconds are used to complete the 10% power level change, the error is reduced to 5 to 10¢ which decays with a 2 to 5 second time constant.

Rapid Power Setback Transients*

For the Demo Plant, consideration is being given to a power setback which would be accomplished by control insertion at the rate of 50¢/second until a power level of 5 to 10% of full power is reached. This requires that about 2.5\$ be inserted at the same time flow is reduced to 5 or 10% of full flow. This procedure presents very special problems to the ORC. First, a static model would lag behind the fuel temperature transient to the extent that errors of 80¢ to 1\$ during the transient are possible. Even after the transient, the decay heat of full power operation is a substantial fraction of the total heat produced after the setback. Heat balance data would be in marked disagreement of the power level indicated by the neutron monitors. The decay heat, even minutes after the setback is complete, causes the fuel temperature to be higher than predicted by a static model which uses the neutron monitors as a basis for power level measurement. The reactivity error due to the increased fuel temperature due to decay heat is 10 to 15¢. The influence of the high gamma source per unit fission power on the structure could be another +1 to +2¢ more than static modeling would anticipate.

To operate with only small errors following a large power setback, the ORC which uses only static modeling requires information about the decay activity as well as the information provided by the neutron monitors.

The above transients are rapid and produce large deviations from the reactivity predictions of a static model. Even though the expected frequency of occurrence of either 10% step changes in power or power setbacks is low compared to the time spent in startup or load following operations, they remain important considerations. Power setback is initiated only under emergency conditions and step changes in power are performed only when more gradual changes are insufficient to meet the demands of the plant. Both have potential for putting large stresses on core components. For these reasons (emergency conditions and potentially high stress), ORC protection during and following these events is extremely desirable for continued safe operation of the plant.

The fraction of operating time in which load following and startup operations are performed is much greater and may amount to a substantial portion of the operating time. The reactivity errors of a static model in these operations are much lower, corresponding to the low rates of change.

*A power setback is an automatic, controlled insertion of the shim rods to reduce the reactor power to levels at which the reactor can be operated after certain events which make continued high power operation unsafe. The advantages of a power setback over a scram are reduced thermal stress of major components and reduced time requirements for returning the plant to high power operation.

Startup and Shutdown Temperature Transients

Temperature transients which are minimized in the power setback, become more important during startup when the 100°F/hr heatup is in progress. The main errors are not likely to be from structural lag in following the heatup unless thermal expansion is an unexpectedly large component of the overall feedback. The thermal time constant of the largest structural elements are less than one minute and so expansion effects can be expected to follow the heatup without a great amount of lag in reactivity effects. The reactivity error of a static model is most probably in the range of 1 to 2¢. Of greater concern is the need for a changing neutron detector calibration factor which is related to the sodium and shielding density between the core and detector. A real time updating of this factor is required to accurately (within a few cents) predict the reactivity changes during heatup or shutdown. The effect of density changes on detector efficiency is discussed in Section C.7.

Conclusions

An ORC which includes a dynamic model of reactor response to the plant variables provides a substantially greater capability to detect malfunctions than a static model due to the speed of detection of an anomalous reactivity during transient conditions. The errors of a static model during transient conditions can be in excess of the reactivities associated with several reactivity effects caused by failure of core components. Transient conditions will exist a significant fraction of the time in LMFBR's for load following, startup, shutdown, and equipment checkout. While many of the transients are slow and have comparatively small deviations from a static model, the errors are directly additive and either increase the likelihood of a false alarm or provide reduced protection due to larger anomalous reactivity limits.

Historically, fuel vendors have reported a higher incidence of fuel failure in water-cooled reactors which experienced load following and similar transients than base load plants so the importance of protection during transient is emphasized by actual experience.

The additional algorithms for the dynamic model must be simple for two reasons. First, the speed of computation must be high for on-line monitoring. Second, the size of the computer used in the ORC will be subject to economic constraints that promote simple computational models.

All of the work that is done in static modeling is also relevant to a dynamic reactivity model. The additional required information, time constants and transport times, is also pertinent to safety analyses. Thus, the dynamic response can be added to the ORC for a minimal expected additional cost in terms of developmental funds. The additional hardware costs appear to be small for the added protection during periods of rapid power change.

C.7 EVALUATION OF RESULTS AND CONCLUSIONS

The incidents postulated in Section 5 would cause anomalous reactivities on the order of 2 to 10¢. More severe incidents would, of course, cause greater reactivity changes, e.g., loss of 50% of the fuel from one assembly leads to ~1\$ reactivity loss. To determine the smallest anomalous reactivity that ORC is required to detect, the comprehensive list of potential malfunctions must be considered, the consequence of failure to detect the event determined, and the ORC accuracy required to detect each event evaluated against the cost and feasibility of obtaining that accuracy. Lacking this analysis, a judgement has been made, based on the events considered in Section 5, that an anomalous reactivity value of ~5¢ should be used in determining ORC accuracy goals. To meet this goal the alarm limit can be set at no greater than 3.5¢ to have a small failure to alarm probability and the ORC reactivity uncertainty can be no greater than ~2¢ to avoid false alarm. These requirements would be met for reactor operation near full power (e.g., 75% to 110% power) and full power operation of no more than one week between rebalancing. A higher alarm setting would cover wider power and burnup ranges, but could detect only the larger anomalies. To meet the goal of a 3.5¢ alarm setting would also require that the development work assumed in estimating the uncertainties after the initial operating interval must be performed, and in some cases even tighter goals obtained. In particular, the reactivity uncertainty for full power operation of one week should be reduced to ~2¢ rather than 3¢, shown in Table C-5. The major areas felt to require most development work are discussed below.

The model described in Section C 2.2 includes all of the significant contributions to reactivity changes in the system. However, in several areas simplifying assumptions have been made or the specific equations have not been developed. These gaps in the model must be filled before the ORC objective can be met and appear to be the next logical steps in developing an anomalous reactivity detection system for the Demo Plant.

Core Component Mechanical Distortion

The model described in Section C 2.2 assumes that the core geometric changes may be related to the temperatures of structural material and fuel by simple thermal expansion equation. However, the effects of radiation induced swelling and creep will cause the core components to tend to distort in a complex manner that will interact with the core restraint system. The resulting geometry of the core is difficult to predict and a modification of the thermal expansion equations will probably be required to adequately predict the reactivity effects from component distortion. It is desired to predict the core geometric reactivity changes accurately enough to limit this source of error in the power coefficient to <2%.

Effect of Control Insertion on Neutron Monitor Readings

As the control system insertion pattern is changed, there can be significant changes in flux distribution, as seen in the ZPPR-3 program. This change in flux shape will cause a change in the relationship of the core power level and the neutron monitoring reading, i.e., a change in calibration of the power range monitors. Therefore, a correction must be applied to the neutron monitor reading before it is used to determine the power level and the temperatures of the core components. Careful modeling of the control effects should reduce this source of error to well below 1%.

Effect of Sodium and Shielding Temperature on Neutron Monitor Calibration

The change in neutron attenuation caused by the changing sodium and shielding material density during large temperature changes effects the apparent detector efficiency and the calibration between neutron monitor reading and core power level. In SEFOR⁽⁷⁾ this effect caused a change of ~6.5% in detector efficiency with a temperature change from 760°F to 550°F. In the Demo Plant, it is anticipated that

there will be more sodium and shielding between the core and the neutron detectors than in SEFOR (~30 inches of sodium) and the temperature dependent calibration problem will be greater. For example, errors on the order of 4 to 6¢ would occur for a 100°F inlet temperature change while at 30% power if the sodium density change were ignored. The effect of density changes is readily predictable and it is expected that inclusion of a simple calibration correction can reduce this error to a negligible value.

Effect of Control Insertion Pattern on Thermal Gradients

The magnitude and even the sign of the local thermal gradients within the core can be changed by the control insertion pattern. The feedback reactivity equations must incorporate a correction of this phenomenon to obtain the desired accuracy of <5% in the power coefficient.

(7) L. D. Noble, et. al., "SEFOR Core 1 Test Results to 20 MW", GEAP-13702, April 1971.

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