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High-Temperature Gas-Cooled Reactor Safety Studies for the Division of Reactor Safety Research Quarterly Progress Report, January 1—March 31, 1980

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Prepared for the U.S. Nuclear Regulatory Commission
Office of Nuclear Regulatory Research
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HIGH-TEMPERATURE GAS-COOLED REACTOR SAFETY STUDIES FOR THE DIVISION OF REACTOR SAFETY RESEARCH QUARTERLY PROGRESS REPORT, JANUARY 1—MARCH 31, 1980

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S. J. Ball, *ORECA-I: A Digital Computer Code for Simulating the Dynamics of HTGR Cores for Emergency Cooling Analyses*, ORNL/TM-5159 (April 1976).

T. W. Kerlin, *HTGR Steam Generator Modeling*, ORNL/NUREG/TM-16 (July 1976).

R. A. Hedrick and J. C. Cleveland, *BLAST: A Digital Computer Program for the Dynamic Simulation of the High Temperature Gas Cooled Reactor Reheater—Steam Generator Module*, ORNL/NUREG/TM-38 (August 1976).

J. C. Cleveland, *CORTAP: A Coupled Neutron Kinetics—Heat Transfer Digital Computer Program for the Dynamic Simulation of the High Temperature Gas Cooled Reactor Core*, ORNL/NUREG/TM-39 (January 1977).

J. C. Cleveland et al., *ORTAP: A Nuclear Steam Supply System Simulation for the Dynamic Analysis of High Temperature Gas Cooled Reactor Transients*, ORNL/NUREG/TM-78 (September 1977).

S. J. Ball et al., *Evaluation of the General Atomic Codes TAP and RECA for HTGR Accident Analyses*, ORNL/NUREG/TM-178 (May 1978).

FOREWORD

High-temperature gas-cooled reactor (HTGR) safety studies at Oak Ridge National Laboratory (ORNL) are sponsored by the Division of Reactor Safety Research, which is part of the Office of Nuclear Regulatory Research of the Nuclear Regulatory Commission (NRC).

This report covers work performed from January 1 to March 31, 1980. Previous quarterly reports and topical reports published to date are listed on p. v. Copies of the reports are available from the Technical Information Center, U.S. Department of Energy, Oak Ridge, TN 37830.

HIGH-TEMPERATURE GAS-COOLED REACTOR SAFETY STUDIES FOR THE DIVISION OF REACTOR SAFETY RESEARCH QUARTERLY PROGRESS REPORT, JANUARY 1—MARCH 31, 1980

S. J. Ball, Manager
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R. M. Harrington

ABSTRACT

Work continued on development of the ORTAP, ORECA, and BLAST codes; and verification studies were continued on the ORECA, CORTAP, and BLAST codes. An improved steam turbine plant model (ORTURB) for use in ORTAP was developed and checked. Predictions from BLAST, CORTAP, and ORECA were compared with various transient test data from the Fort St. Vrain reactor.

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1. HTGR SYSTEMS AND SAFETY ANALYSIS

S. J. Ball

Work for the Division of Reactor Safety Research under the High-Temperature Gas-Cooled Reactor Systems and Safety Analysis Program began in July 1974, and progress is reported quarterly. Work during the present quarter included further work on code verification and development.

1.1 Development of the Fort St. Vrain (FSV) Nuclear Steam Supply System Simulation Code ORTAP-FSV

J. C. Conklin

The steam turbine model ORTURB has been rewritten to improve the prediction of off-normal transients such as those involving loss of condenser or loss of feedwater heater. This present model uses governing equations similar to those presented for the steam turbine model in ref. 1, but uses a modeling and iteration scheme that minimizes floating point exponentiation, a notorious consumer of computer time.

The basic governing equation for the pressure and flow balance of both the high- and low-pressure turbines is the ideal gas flow law,

$$W = A \left\{ \frac{k}{k-1} \left(\frac{P_1}{v_1}\right) \left[\left(\frac{P_2}{P_1}\right)^{2/k} - \left(\frac{P_2}{P_1}\right)^{(k+1)/k} \right] \right\}^{1/2} ,$$

where

k = ratio of specific heats,

P = pressure,

v = specific volume,

A = represents a flow constant, dependent mainly on area,

W = mass flow.

The subscript 1 refers to an upstream value, and the subscript 2 refers to a downstream value.

Use of this equation allows the effect of a downstream pressure variation to be reflected upstream when the pressure ratio is greater than critical, an important consideration for loss-of-condenser and loss-of-feedwater-heater transients; the equation also allows correct prediction of the transient performance of the high-pressure turbine, whose exhaust pressure will be affected by the steam turbines driving the helium circulators.

The intermediate- and low-pressure turbine (ILPT) stages are divided into seven groups, with boundaries at the steam inlet, the condenser, and six extraction points. The flow constants and stage group thermal efficiencies are calculated from design input data. The flow constants are assumed constant throughout the simulation, and the stage group efficiencies are corrected for turbine inlet volume flow.

The extraction flows are calculated at each iteration by assuming that the pressure drop between the turbine extraction point and the feedwater heater shell is caused by form drag, where the proportionality constant is calculated from design input data.[2] The feedwater heater shell pressure is assumed constant throughout the turbine iterations for each time step.

After initialization calculations, a computational sweep is performed to calculate turbine flows from the pressure distribution. The mass flows are then checked at the extraction points to ensure that they are balanced within a given tolerance. If the flows are found to be unbalanced at one extraction point, the pressure at that point is appropriately modified, and resultant mass flows are calculated for the stage groups both upstream and downstream of the extraction point in question. The turbine flows are again checked, and if all are balanced within the tolerance, the turbine iterations are completed. If the flows are again found not to balance at any extraction point, this "two-point" iteration process is repeated. This technique minimizes the floating point exponentiation made necessary by the ideal gas flow equation.

Test cases were run in which the entire turbine flow distribution was recalculated if only one of the stage group boundary pressures needed modification during the iteration process. No significant differences in converged flows were noticed between this and the "two-point" iteration case, but the computing time was greatly increased.

The feed pump turbine has been modeled as one stage group, so the entrance and the exit pressures are used as the two points in the ideal gas flow equation. The entrance pressure is set equal to the pressure at the second ILPT extraction point, and the exit pressure is set to the main condenser pressure. No flow control device has been modeled for the feed pump turbine, so the flow through it is dependent on the inlet steam conditions and the outlet steam pressure, with less than critical flow. A flow control device could easily be added if required for a specific transient.

The low-pressure turbine exhaust loss is calculated according to the procedure developed by Spencer et al.[3] This loss is a unique function of the steam velocity at the discharge of the last stage bucket. Empirical data used for this procedure were developed from known dimensions (85.1-cm active length on the last stage bucket) and published exhaust losses[4] at 100 and 25% power.

The high-pressure turbine has been divided into three stage groups: the flow control valve(s) and governing stage, and two reaction stage groups. This detail and the resulting additional computational expense were necessary for proper calculation of the governing stage shell pressure, which is a feed-forward signal used in the overall plant control system and is primarily determined by the flow-passing ability of the following reaction stages.

The governing stage shell pressure is determined from initial conditions at 100% power. The governing stage is assumed to be designed according to the method described by Salisbury.[2] The ratio of governing-stage shell pressure to the design exit pressure at 100% power was less than the critical pressure ratio. The following reaction stages were then modeled as two stage groups so that the pressure ratio of each was greater than critical. Thus, downstream exit pressure variations, which affect the flow-passing ability, could be reflected upstream to the governing-stage shell pressure.

The high-pressure turbine thermal efficiency is calculated from input data at 100% power and corrected for off-normal conditions by the methods presented by Spencer et al.[3] Two important design factors, the governing-stage pitch diameter (76 cm) and the number of rows of moving buckets of the governing stage (1), were obtained by applying the methods and information from ref. 3 to the published heat balances[4] at 100 and 25% power.

The electric power produced by both turbine generators is the sum of all the products of the stage-group mass flow and enthalpy differences. The flow constant of the governing stage is varied in order to control the mass flow through it in a manner analogous to the main steam control valve(s). Flow into the ILPT is not controlled; thus, the steam inlet conditions determine the flow.

Turbine runback transients from 100 to 25% power were simulated, and the steady-state results are in excellent agreement with published heat balances.[4] Also, transients representing loss of condenser, loss of feedwater heater, and high pressure turbine exit pressure fluctuations were simulated. The turbine model yielded appropriate responses for all simulated transients. However, the feedwater heater model calculated inappropriate values. This problem area is being investigated presently.

The ORTURB turbine model uses approximately 0.05 s of IBM Model 360/91 computer time for each computational time step. This value is subject to the transient modeled and will increase as the severity of the transient increases. However, this is a significant improvement in computer time as compared with the earlier turbine model in ORTAP.[1]

1.2 Development of the Steam Generator Code BLAST

J. C. Cleveland

Comparison of BLAST code[5] predictions with measured plant data is proceeding in two areas. The BLAST predictions are currently being compared with FSV transient data obtained during an oscillation test transient that caused a rapid decrease in helium inlet temperature to a particular steam generator module in loop 1. Also, comparison of BLAST predictions with data obtained from the West German Arbeitsgemeinschaft Versuchsreaktor (AVR) steam generator is scheduled to begin in May 1980. This comparison is being performed by Rheinisch-Westfälischer Technischer Überwachungs-Verein e.V. (RWTUV) in cooperation with Kernforschungsanlage (KFA). A model of the AVR steam generator using BLAST has been completed by RWTUV. The initial comparisons will be made for steady-state conditions. This code is also being used for the steam generator part of the Thorium High-Temperature Reactor (THTR) plant simulation being developed cooperatively by RWTUV and KFA.

The FSV oscillation test transient of Nov. 4, 1978, involved a large [~44°C (80°F)], rapid decrease in helium inlet temperature to steam generator module B-1-1 in loop 1 (Fig. 1). This resulted in a drop in main steam subheader temperature of approximately 68°C (122°F) for this module. The purpose of this analysis is to make a direct comparison of BLAST predictions with the measured plant response. Analysis of the transient is not complete, but the following is intended to indicate the nature of the preliminary results obtained to date.

For the oscillation transient, General Atomic (GA) provided measured data for reactor power, loop 1 feedwater flow, total core helium flow, module B-1-1 helium inlet temperature, loop 1 inlet and outlet reheat steam temperature, and module B-1-1 subheader outlet steam temperature vs time. Some inputs required for BLAST (e.g., feedwater temperature

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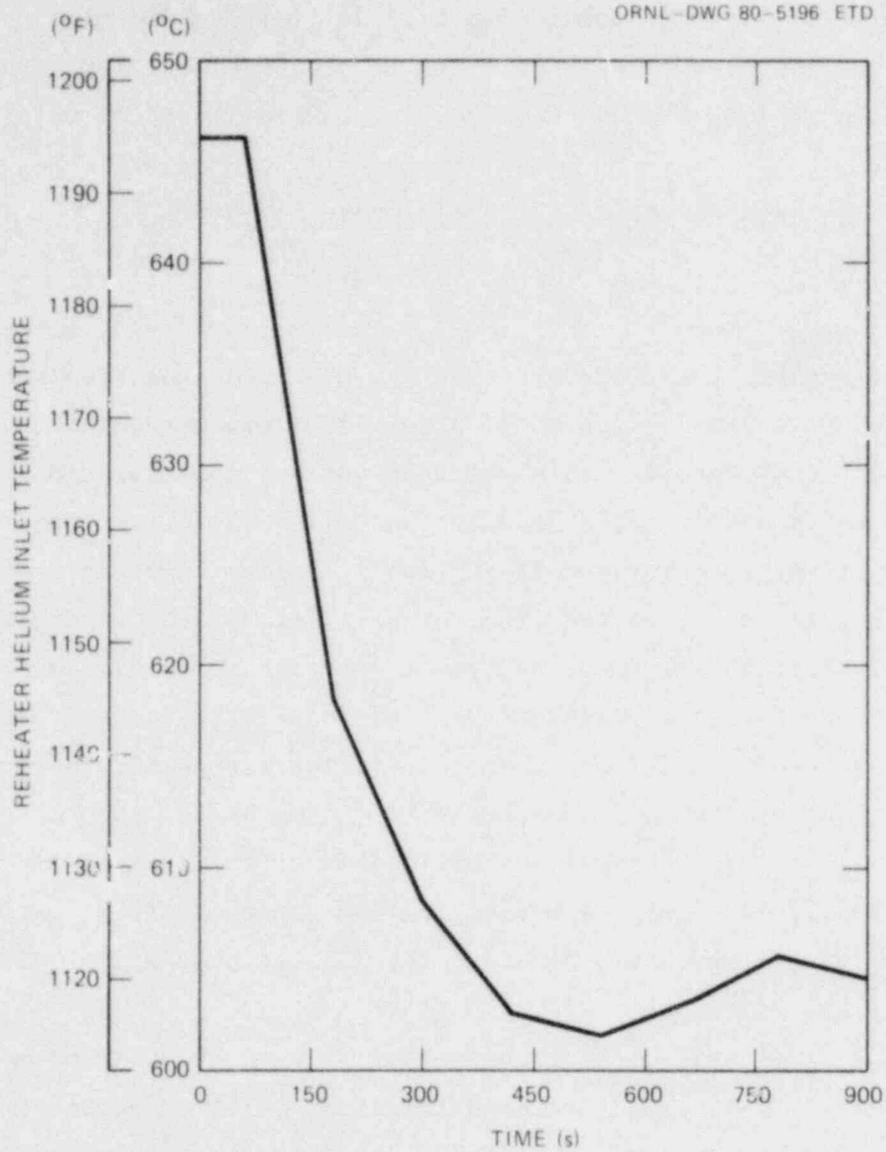

Fig. 1. Helium inlet temperature, module B-1-1, during oscillation transient.

and pressure, reheat steam flow and pressure, and main steam pressure) were not provided and have been estimated by interpolating from steady-state conditions expected at 25 and 100% power. Additionally, loop 1 feedwater flow and reheat steam flow were assumed to be distributed equally among the six steam generator modules in loop 1.

Figure 2 shows the nodal arrangement used in analyzing this transient. The model uses ten water nodes, ten tube nodes, and seven helium nodes.

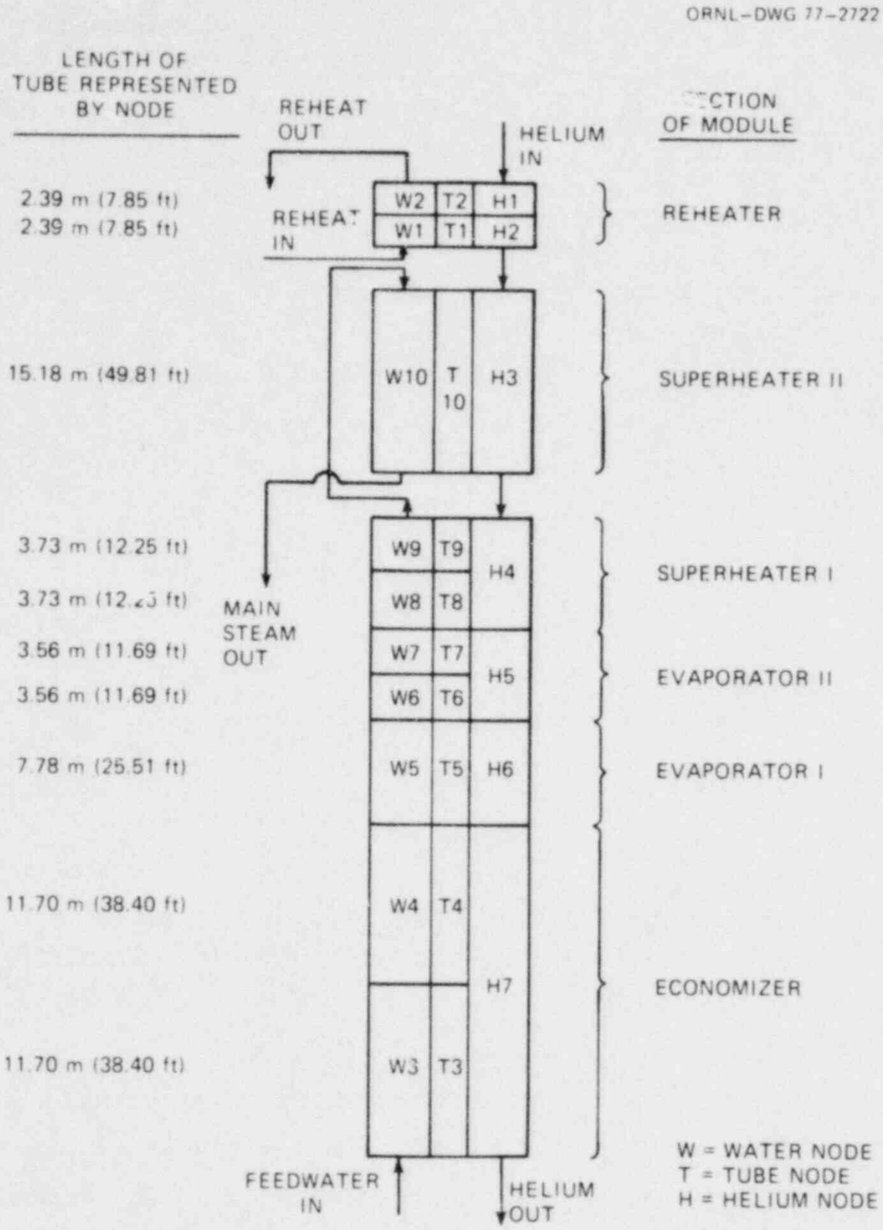

Fig. 2. Nodal arrangement for BLAST simulation of FSV steam generator.

BLAST allows up to 20 nodes of each type, but the model used has shown good agreement with BLAST calculations using more nodes.

Figure 3 shows a comparison of the change in superheater-II exit steam temperature as computed by BLAST vs the change in measured subheader outlet temperature for module B-1-1.* The flow-dependent lag

*Superheater II exit temperature is not measured for module B-1-1.

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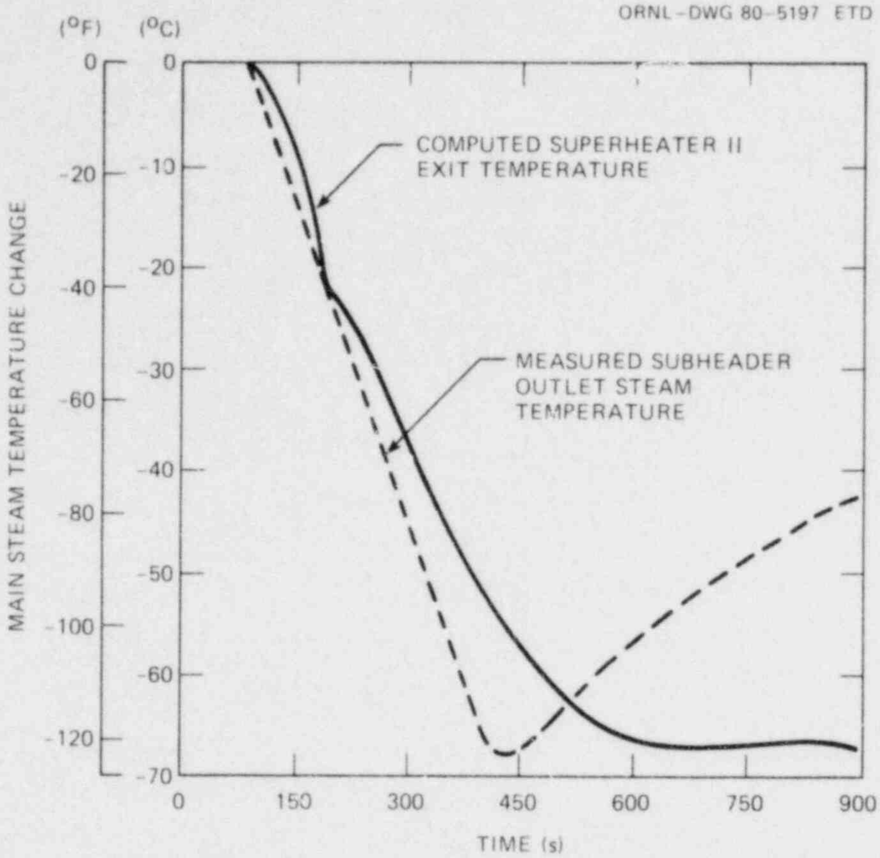

Fig. 3. Com, :rison of computed superheater II exit steam temperature with measured subheader outlet steam temperature for module B-1-1.

associated with the steam temperature measurement has been incorporated into the BLAST prediction. As is shown, the calculated drop in steam temperature resulting from the 44°C drop in helium inlet temperature was ~67°C (121°F) and compares very well with the measured steam temperature drop. However, the measured data showed that the initial drop in steam temperature was followed by a 25°C (45°F) increase in steam temperature, which is not reflected in the BLAST calculations. The increase may result from differences between actual conditions (e.g., transient feedwater flow, feedwater inlet temperature, and main steam pressure for module B-1-1) and estimated inputs to BLAST (the estimated values used in BLAST for these parameters were assumed to remain constant during the transient.) Furthermore, while the computed temperature changes during the transient compare fairly well with measured data, there is an "offset" at the time of transient initiation between the computed superheater-II exit temperature and the measured subheader temperature of ∿42°C (75°F) (based on current inputs to BLAST, some of which are assumed values), with the computed temperature being higher than the measured value. Reasons for this offset have not been explored in depth to date. A significant portion of this offset could possibly be attributed to regenerative heating, which causes the subheader outlet main steam temperatures to be lower than superheater II exit temperatures.

The first step in examining the reasons for the differences between the preliminary BLAST computation and measured data for the oscillation transient is to attempt to obtain data for those input values that have had to be estimated for these initial BLAST calculations. Specifically, data are needed for (1) feedwater inlet temperature, pressure, and flow (to module B-1-1), (2) main steam pressure; and (3) reheat steam flow and pressure during the transient. To explore the "offset" discussed previously, it would be beneficial to obtain data for steady-state temperature distributions within the steam generator from measurements of conditions within the highly instrumented steam generator module (module B-2-3). Both superheater II exit and subheader outlet temperatures are measured in module B-2-3. Evaluation of these data will provide a detailed comparison of temperature calculations and measurements at various positions within the reheater and steam generator bundles on both helium and water-steam sides. Requests for both types of data are being made to GA. Evaluation of these data will indicate the need for model modifications and for simulation of regenerative heating within the steam generator.

Other BLAST development work involved modification of the technique for computing bundle outlet enthalpy based on conditions in the last node in the bundle. The modifications resulted from an evaluation of anomalous results obtained by RWTUV for a transient involving very low reheat steam flows (and consequently very long nodal transport times). The modified technique is consistent with that used in RETRAN.[6] The modification was tested on several cases at ORNL and has been provided to RWTUV for consideration and testing.

Answers to a few additional questions from RWTUV related to use of BLAST were provided to RWTUV.

1.3 Comparisons of CORTAP Code Calculations with FSV Transient Data

R. M. Harrington

The CORTAP[7] calculation of reactor power transients resulting from control rod movement has been verified by comparison with operating data taken during control rod influence tests at FSV. This code calculates the reactor power and representative fuel, moderator, and coolant temperatures. Inputs are (1) coolant temperature, (2) flow and pressure at the core inlet, and (3) control rod reactivity. The CORTAP code was used as an independent calculation of core response for work reported here. It is a subroutine of the plantwide simulation ORTAP.[1]

The tests[8] were conducted during January 1978 at a power level of 50%. Each test consisted of a brief control rod insertion or withdrawal followed by constant control rod position throughout the remainder of the test. Two test transients were used: a 6-s withdrawal of region 1 control rods and a nominal 24-s insertion of region 6 rods. Control rod speed was 2.5 cm/s in both cases. Control rod worths were such that a 15-cm change in the region 1 control rod position changed reactivity more than the 61-cm change in region 6 rod position.

The reactor power transient was recorded for each of the six neutron detectors. Data from the six detectors were averaged for comparison with the CORTAP calculation of reactor power response. No attempt was made to compare region outlet temperatures with CORTAP calculations because the time response of these thermocouples is not known with sufficient accuracy.

Several important CORTAP input parameters that were used are summarized in Table 1. To get good comparison with the January 1978 data, beginning-of-cycle (BOC) initial core neutron kinetics data were used. Core flow was calculated from steady-state core inlet and outlet temperatures reported in the test data. Since control rod travel was

Table 1. Parameter values used for CORTAP comparison with experimental data

Parameter	Value	Source
Neutron kinetics		
β_i	BOC, initial load	Table 3.5-10, ref. 10
λ_i	BOC, initial load	Table 3.5-10, ref. 10
Doppler coefficient	BOC, initial load	Fig. 3.5-11, ref. 10
Moderator coefficient	BOC, initial load	Fig. 3.5-13, ref. 10
Axial power shape	BOC, initial load	Fig. 3.5-10, ref. 10
Thermal		
Moderator specific heat	Fit to GA data	Ref. 11
Moderator conductivity	57 $W/(m^2 \cdot °C)$ (radial)	Ref. 10
Moderator conductivity	0.0 (axial)	Ref. 7
Heat transfer coefficient, fuel moderator gap	2270 $W/(m^2 \cdot °C)$	Ref. 7
Fuel specific heat	Fit to GA data	Ref. 11
Fuel conductivity	23 $W/(m^2 \cdot °C)$ (radial)	Ref. 11
Fuel conductivity	0.0 (axial)	Ref. 11

short in comparison with the 4.5-m active core length, constant differential rod worth was assumed for input to CORTAP. Core flow and inlet temperature were assumed to remain constant throughout each test. Transient data for these variables was not reported in ref. 8.

Flux-squared weighting was incorporated into CORTAP to account for the axial flux distribution in calculating reactivity feedback caused by nodal temperature changes.[9] The reference version of CORTAP weights the reactivity feedback only by nodal volume fraction; therefore, it should be conservative. A test of the conservatism was made by simulating a severe rod withdrawal transient ($1./min reactivity insertion for 90 s with reactor protection system disabled). The results show that the reference version predicts a slightly higher peak power:

Time, s	0	30	60	90
Relative power (modified), P/P_0	1.0	1.95	2.38	2.74
Relative power (reference version), P/P_0	1.0	2.02	2.49	2.85

Before performing the desired CORTAP transient comparisons with the FSV tests, runs were made to determine the required temperature interval for recomputation of matrix elements in the core thermal model (pages 27 and 28 of the CORTAP report).[7] An interval of 11°C led to a steady-state offset of ∿16% between calculated reactor power level change and core thermal power output change. With a value of 3°C, the offset was eliminated.

The single most important parameter — total reactivity added by control rods — was not reported for either test. In using the same data for validating the code BLOOST, GA used the code GAUGE to calculate how much reactivity the control rod movement added but did not report this intermediate result in ref. 8.

For the ORNL validation of CORTAP, the decision was made to infer how much reactivity was added as a result of each control rod movement. First, CORTAP was used to calculate parameter sensitivities. The base transient used to calculate these sensitivities was a 6-s reactivity insertion. For the same transient each one of seven key parameters was changed individually:

1. total control rod reactivity, ρ,
2. moderator specific heat, c_{pm},
3. fuel specific heat, c_{pf},
4. fuel conductivity, k_f,
5. fuel-moderator gap conductance, h_{gap},
6. core flow, W_{ac},
7. Doppler coefficient, α_f.

The results are shown in Fig. 1, expressed as normalized sensitivity:

$$S = \frac{\Delta(\phi - \phi_0)/(\phi - \phi_0)}{\Delta P/P_0} ,$$

where

ϕ_0, ϕ = initial (time zero) and transient reactor power,

P_0, P = reference and perturbed valve of parameter of interest.

As seen in Fig. 4, during the first 6 s, while the control rods are in

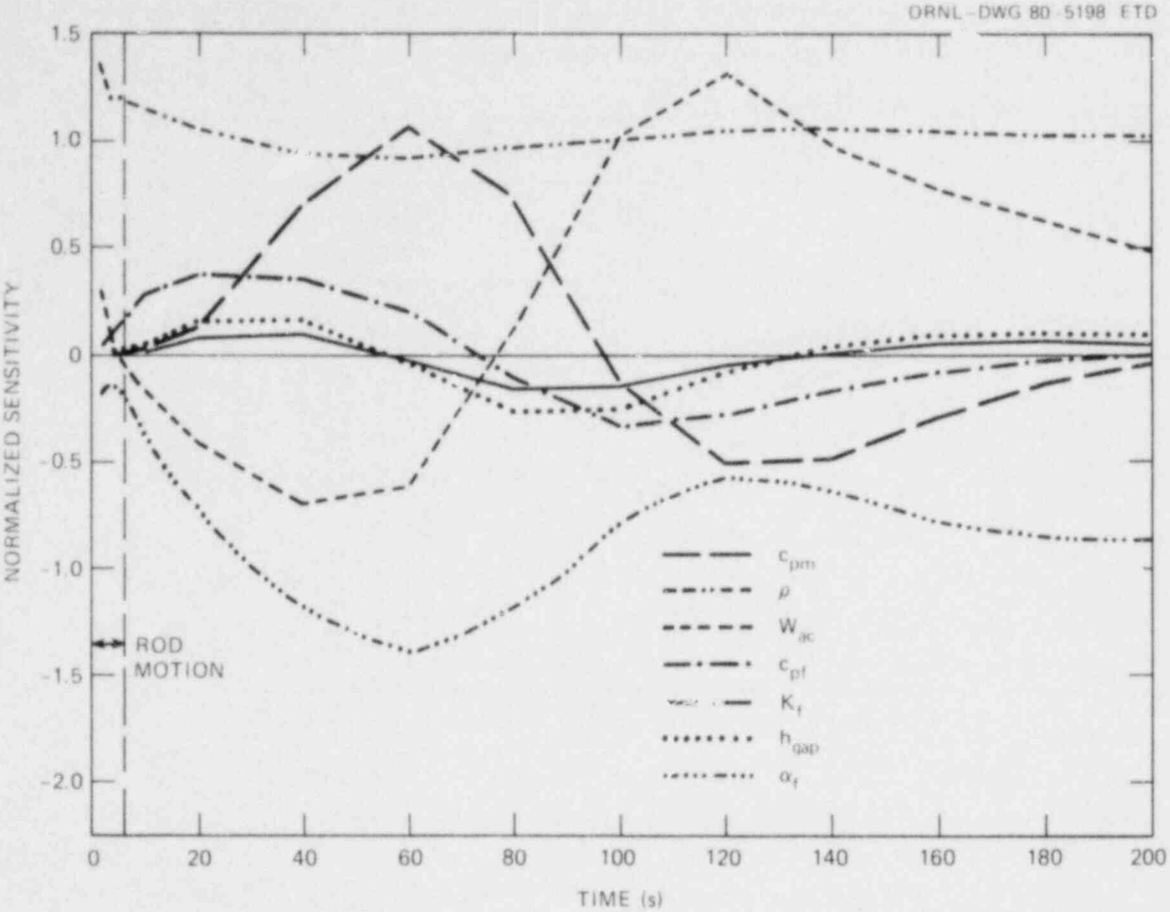

Fig. 4. Parameter sensitivity of reactor power response following 9.5¢ reactivity insertion.

motion, sensitivity to control rod reactivity is five to ten times greater than any of the other parameters. After the control rods stop moving, the other parameters become more important. The fuel and moderator specific heats have an effect on reactor power during the dynamic part of the transient but no effect on the final steady-state power level reached. The Doppler coefficient and coolant flow have a significant effect on both the dynamic portion and final steady-state power change. The sensitivity to fuel, moderator, and fuel-to-moderator gap conductivities seems small because most of the thermal resistance between fuel centerline and coolant is due to the relatively large resistance of the moderator-to-coolant film coefficient. This is illustrated in Table 2, which shows steady-state temperatures calculated by CORTAP for a 50% power level.

Table 2. CORTAP calculation of steady-state temperature distribution in average channel at 50% power

Axial position[a] (cm)	Temperature (°C)						
	Fuel node				Moderator node		Coolant
	1	2	3	4	1	2	
37.5	468	466	462	457	434	427	361
112.4	569	566	560	552	518	508	412
187.3	676	673	666	655	613	600	480
262.4	717	713	708	698	663	652	550
337.3	752	750	744	737	706	697	610
412.2	756	754	751	745	723	717	657

[a]Distance from top of active fuel region.

If the other parameters are known reasonably well, then the control rod reactivity could be inferred by simply matching experimental and calculated responses during the first 6 s (or during the first ~24 s for the rod insertion transient). This is the procedure that was used to calculate control rod reactivity for the comparisons reported in the following paragraph.

Results of the CORTAP calculation of reactor power and the corresponding plant data are shown in Fig. 5 for the 15-cm control rod withdrawal and in Fig. 6 for the 53-cm control rod insertion. The agreement between experiment and prediction is good, both for the transient portion and for predicting the final steady-state power change. In fitting CORTAP results to determine input reactivity of the nominal 61-cm rod insertion, an insertion of 53 cm better matched the data. This is consistent with the 61-cm technical specifications limitation on control rod travel mentioned in ref. 8. For both rod insertion and withdrawal, CORTAP calculated that the power change at ~90 s would slightly undershoot the final steady-state power change. The plant data

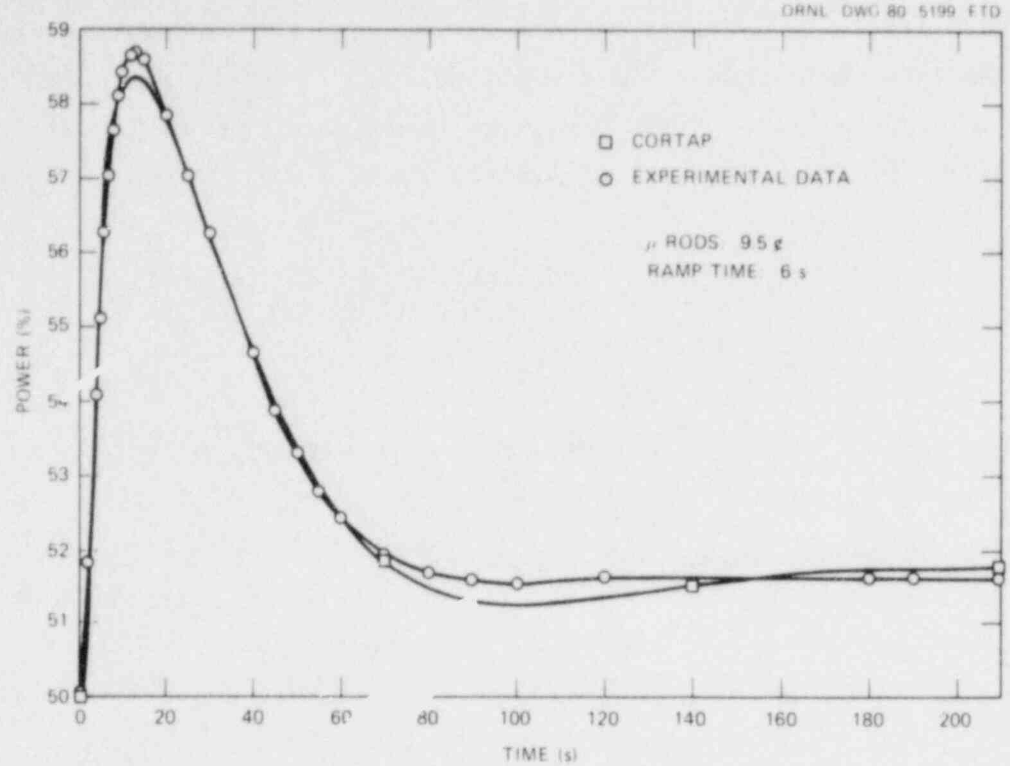

Fig. 5. Reactor power response following 9.5¢ reactivity insertion.

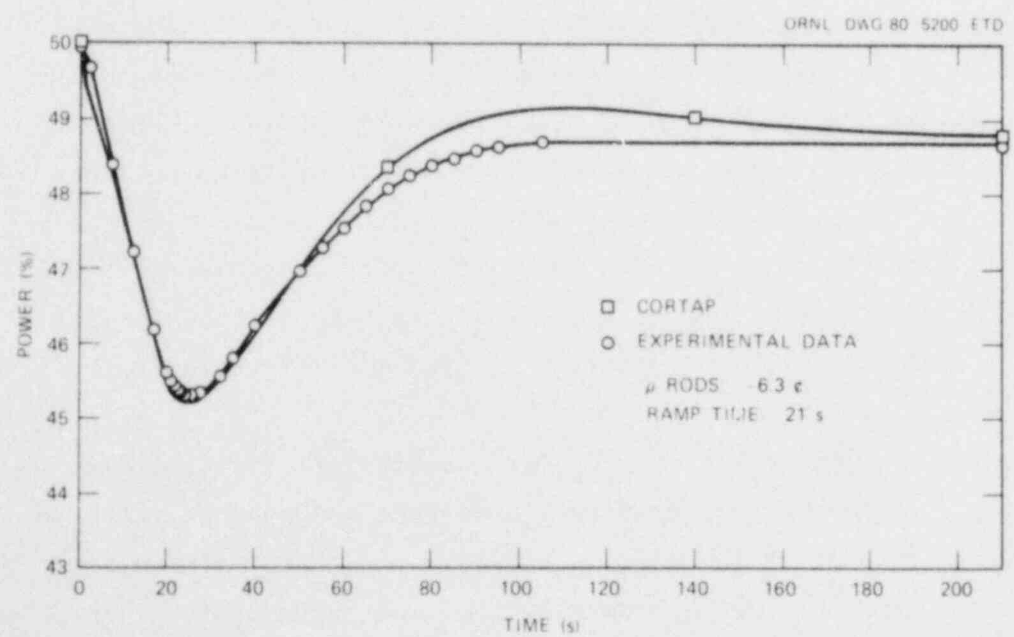

Fig. 6. Reactor power response following −6.3¢ reactivity insertion.

show very little tendency to undershoot the equilibrium power level. The same data[7] were used by GA for validation of BLOOST, with the result that BLOOST predicts an undershoot very similar to the CORTAP calculations. The reason for the undershoot phenomenon remains unexplained.

1.4 Development of the ORECA Code and Comparisons of Calculations with Data from FSV Scram Tests

S. J. Ball

The ORECA code,[12] which models the three-dimensional core temperature and flow distribution in the FSV reactor, was modified to accept an alternate set of initial conditions. In the original version, inputs of core total power and region peaking factors, along with region inlet and outlet temperatures, were used to derive region flows. From these flows, an initial set of region orifice coefficients was derived and assumed unchanged for the duration of the transient. The modified version allows the option of using region orifice coefficients as derived from orifice position readings. In this case, an iterative calculation is used to find the initial core power and region peaking factors.

Comparisons of ORECA code predictions vs FSV scram test data were made using the region orifice position data to derive initial conditions, as noted previously. Optimizations using this new feature were in general not as successful as the original method, however, because individual peaking factor estimates could not be adjusted to accommodate the observed variations in region time constants.

Additional data on the four FSV scram tests being used for ORECA code verification were received from GA. The data include initial values of core differential pressure and plots of steam generator module helium inlet temperature vs time.

Previously, optimization calculations for rationalizing ORECA calculations with FSV test data have been made independently for each of the four scram tests. The most sensitive parameters in the optimization are the estimates of core bypass flow fraction and individual refueling region initial peaking factors, all of which are very much dependent on the particular run conditions. With the new ORECA initial condition option noted previously, which allows use of orifice position data, plus the new data on initial core pressure drop, an ORECA optimization could be done to search for a single best value of effective core bypass flow resistance (as opposed to bypass flow fractions, which would depend on region orifice settings and other conditions). Supposedly, this bypass resistance would be valid for all four scram tests. Preliminary analyses of the data, however, indicated that use of a single effective value of core bypass flow restriction coefficient for all four scram tests would not be feasible, because the ORECA calculations implied that the bypass restriction decreased by about an order of magnitude between the initial and final tests (Table 3). Preliminary indications from FSV cycle 2 data, however, are that the core bypass flow fraction is much lower and closer to design values than it was before the region constraint devices were installed.

Table 3. ORECA calculations of FSV effective core bypass flow fractions and resistance coefficients for four cycle 1 scram tests

Scram test date	Initial power (%)	Initial core ΔP [kPa (psi)]	ORECA estimates	
			Core bypass fraction	Bypass flow resistance
7/23/77	30	14.4 (2.09)	0.05	12
8/6/77	29	12.5 (1.81)	0.06	8
10/25/77	40	14.1 (2.04)	0.10	2
5/8/78	50	17.7 (2.57)	0.11	1

Preliminary ORECA calculations were also made to compare predicted and measured steam generator helium inlet temperatures for the four scram tests. In some cases several of the inlet temperatures would rise initially in response to a simultaneous scram and loop trip. Analysis of the data showed that such rises were probably due to the sudden core lower plenum flow redistributions that would occur on a loop trip. This also led to a likely explanation of why some refueling region outlet temperatures rose initially after a scram. For example, the rise in region 20 temperature after the 5/8/78 scram[13] is probably due to the redistribution of flows from regions 7, 18, 19, and 36 (which averaged ~160°C hotter than region 20), which would exit the plenum near region 20 after a loop 1 trip. Further analysis of the steam generator data is planned.

1.5 Documentation of the FLODIS Code

J. C. Conklin

A paper entitled "Thermal-Flow Performance of the Fort St. Vrain High Temperature Gas-Cooled Reactor Core During Two Design-Basis Accidents"* has been accepted for presentation at the American Nuclear Society/American Society of Mechanical Engineers topical meeting on Nuclear Reactor Thermal-Hydraulics scheduled for October 6-8, 1980, in Saratoga, New York. The abstract is as follows:

The Fort St. Vrain 330 MW(e) HTGR was designed and built by General Atomic Company and is operated by Public Service Company of Colorado. FLODIS, an ORNL computer code written specifically to analyze the core thermal-flow response of this reactor, was used to investigate two postulated design basis accidents: the design basis depressurization accident (DBDA), and the loss of forced convection (LOFC) accident. FLODIS can calculate the distribution of flow among the 37 refueling regions and the internal flow distribution within the individual refueling regions.

The sensitivity of the interregional core flow distribution due to the position of the flow control orifices was investigated. The effect of temperature on helium viscosity is an important factor in the interregional and intraregional flow redistribution subsequent to both accidents.

*A significant portion of this paper was previously published in ref. 14.

2. CONFERENCE ATTENDED UNDER PROGRAM SPONSORSHIP: FOURTH POWER PLANT DYNAMICS, CONTROL, AND TESTING SYMPOSIUM, GATLINBURG, TENNESSEE, MARCH 17–19, 1980

S. J. Ball

A paper entitled "Dynamic Model Verification Studies for the Thermal Response of the Fort St. Vrain HTGR Core" was written, presented, and published in the proceedings of the previously mentioned conference. The abstract of the paper is as follows:

The safety research program for high-temperature gas-cooled reactors at ORNL is directed primarily at addressing licensing questions on the Fort St. Vrain reactor near Denver, CO. An important part of the program is to make use of experimental data from the reactor to at least partially verify the dynamic simulations that are used to predict the effects of postulated accident sequences. Comparisons were made of predictions with data from four different reactor scram (trip) events from operating power levels between 30 and 50%. An optimization program was used to rationalize the differences between predictions and measurements, and, in general, excellent agreement can be obtained by adjustment of models and parameters within their uncertainty ranges. Although the optimized models are not necessarily unique, results of the study have identified areas in which some of the models were deficient.

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