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12.0 CONDUCT OF OPERATIONS

12.1 GENERAL

The Kewaunee Nuclear Power Plant (KNPP) is jointly owned by Wisconsin Public Service Corporation (WPSC), Wisconsin Power and Light Company, and Madison Gas and Electric Company.

On August 7, 2000, the Nuclear Management Company LLC (NMC) assumed responsibility for operating KNPP. The transfer of operating authority from WPS to NMC was approved by the NRC in References 1 and 2.

Prior to operating license transfer WPS had responsibility for operating KNPP. WPS has had a long history of building and operating safe electric generating plants. In operating its first nuclear plant, WPS applied proven managerial techniques, which were successful in enforcing the highest operating standards. It was WPS's policy to operate the plant in such a manner that it:

1) will not in any way endanger the health and safety of the public,

2) will be safe for the employees to operate,

3) will comply with the applicable rules and regulations of the NRC and other federal and state agencies, and

4) will efficiently provide a reliable supply of electric power to all the customers relying on the plant for service.

These objectives have been successfully met since the plant commenced commercial operation in 1974, and will continue to be met by WPS and the NMC.

REFERENCES – SECTION 12.1

1. Letter from CF Lyon (NRC) to ML Marchi (WPS), transmitting the NRC Order and Safety Evaluation Report (SER) approving transfer of operating authority for the Kewaunee Plant from WPS to the Nuclear Management Company LLC (NMC), Letter No. K-00-044, dated May 15, 2000

2. Letter from T. Kim (NRC) to MB Sellman (NMC), issuing Operating License Amendment No. 149, reflecting the transfer of operating authority to NMC, Letter No. K-00-068, dated August 7, 2000

12.2 ORGANIZATION AND RESPONSIBILITIES

The information contained in this section has been superseded by the Operational Quality Assurance Program Description (OQAPD). OQAPD revisions are prepared as required by 10 CFR 50.54(a).

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12.3 TRAINING

12.3.1 PREOPERATIONAL TRAINING

By the time the plant was ready for startup, key plant supervisory personnel had undergone thorough training. Training given each supervisor, along with previous education and experience, provided assurance that supervisors were qualified to fulfill their responsibilities for safe and efficient operation of the plant. Training was also provided to operating and maintenance personnel and technicians as required for the safe and efficient performance of their duties. All on-site personnel were trained in Emergency Plan procedures to assure their safety and the health and safety of the public. ANSI N18.1, 1971, was used as the basis for developing specific training programs.

12.3.2 RETRAINING

To assure the continuing competence of the operating personnel, formal retraining and replacement training meets or exceeds the requirements and recommendations of ANSI N18.1, 1971, and 10 CFR 55. This program consists of self-study, classroom study, and oral and written examinations. Material covered includes systems design and operation, operating and emergency procedures, radiation safety and health physics, and infrequent operations, such as refueling.

A NRC approved operator re-qualification program is currently in use and is designed to ensure that all licensed Reactor Operators and Senior Reactor Operators maintain the proficiency to continue to operate the Kewaunee Nuclear Power Plant safely, efficiently and within the requirements of the Technical Specifications.

12.3.3 EMERGENCY PLAN AND OTHER SIGNIFICANT TRAINING

An Emergency Plan has been prepared and submitted to the NRC as a separate document. On-site personnel receive Emergency Plan training. Emergency exercises are held in accordance with Appendix E, 10 CFR 50, to evaluate the response of emergency personnel to various simulated emergencies. Training is also given to off-site personnel to enable them to respond quickly and efficiently to support the plant staff for any abnormal situation that might arise. Training requirements are detailed in the KNPP Emergency Plan.

Special fire-fighting training is given to the Fire Brigade, on a quarterly basis, in accordance with Section 27 of the NFPA Code 1975, and fire drills are conducted to check the readiness of these personnel. Appropriate operations personnel are part of the Fire Brigade and also participate in firefighting training.

Industrial safety training is given to all plant personnel and includes other topics applicable to the nuclear plant. First aid training is given to select plant personnel who are called in the event of an injury.

A General Employee Training Manual has been prepared to provide general information to all personnel. The manual includes topics such as plant organization and administration, industrial safety, fire protection, quality assurance, plant security and fitness for duty, and radiological protection.

12.4 PROCEDURES

12.4.1 NUCLEAR ADMINISTRATIVE DIRECTIVES

Nuclear Administrative Directives (NADs) are prepared to provide guidance to plant and corporate staff for activities directly associated with plant operation. These directives are reviewed and approved by the appropriate individual or group, as required by the OQAPD. Review of NADs for consistency with the OQAP is the responsibility of the Site Quality Assurance Manager.

The following directives are typical of those prepared for the administration of the plant:

- Records Management
- Receipt and Inspection
- Material Storage
- Tagout Control
- Kewaunee Assessment Process (KAP)
- Radiation Work Permits
- Maintenance Work Requests
- Operations Group Organization

12.4.2 OPERATING PROCEDURES

Operating Procedures are prepared to cover the operation of the plant including integrated and system startup, normal operation, shutdown, and refueling, and various postulated emergency and abnormal conditions.

In response to Item I.C.6 of NUREG-0737, a review of plant operational verification procedures was performed to ensure that an effective system of verifying the correct performance of operating activities exists and provides an adequate means of limiting human errors and improving the quality of normal operations. Following procedural and staffing changes made in an effort to satisfy the requirements of this item, the NRC concluded in Reference 7 that Kewaunee was in full compliance with Item I.C.6 of NUREG-0737.

The Operating Procedure review and approval is the responsibility of the appropriate individuals, as required by the OQAP and applicable NADs. A checkoff after reading record system is used to ensure that all operations personnel note changed Operating Procedures. Integrated Plant Emergency Operating Procedures (IPEOPs) are reviewed at least every two years. Checkoff lists are used where applicable to assure adherence to written procedures.

The following Operating Procedures are typical of those prepared for operating the plant:

<u>Integrated Plant Operating Procedures</u>

- Plant Startup from Cold Shutdown to Hot Shutdown

- Plant Shutdown from 35% Power to Hot Shutdown Condition
- Plant Cooldown from Hot Shutdown to Cold Shutdown

System Operating Procedures

- Boron Concentration Control
- Residual Heat Removal System Operation
- Reactor Coolant Pump Operation
- Feedwater System Normal Operation
- Air Removal System
- Turbine Oil Purification
- 480 Volt A-C Supply and Distribution System
- Radiation Monitoring System
- Makeup Water System

Abnormal Operating Procedures

- Malfunction of Reactor Makeup Control
- Reactor Coolant Leak
- Failure of Rod Control Bank to Move
- Abnormal Radiation Monitoring System

Integrated Plant Emergency Operating Procedures

- Reactor Trip or Safety Injection
- Loss of Reactor or Secondary Coolant
- Steam Generator Tube Rupture
- Natural Circulation Cooldown

12.4.3 SURVEILLANCE PROCEDURES

Surveillance Procedures are prepared to cover the implementation of the Technical Specifications, Section 4.0 Surveillance Requirements. A checklist is filled out and reviewed for each surveillance test. A KAP form is initiated to document problems that prevent the normal running of the surveillance test and the subsequent resolution of that problem.

The performance of the various surveillance tests has been delegated to the operations, maintenance, instrument and control, rad-chem, and engineering areas of the nuclear organization. The appropriate individuals are assigned responsibility for the review and approval of the Surveillance Procedures, as required by the OQAP and applicable NADs.

The following Surveillance Procedures are typical of those prepared for a particular surveillance test:

- Nuclear Power Range Channel Gain Adjustment

- Reactor Coolant Temperature Instrument Channel Test
- 4 kV Voltage and Frequency Test
- Containment Pressure Instrument Channel Test
- Control Rod Drop Time Test - Startup Measurements
- Reactivity Anomalies

12.4.4 MAINTENANCE PROCEDURES

Maintenance Procedures are prepared to perform mechanical and electrical preventative and corrective maintenance, except for periodic maintenance specifically identified in the surveillance procedures.

Post-maintenance testing of all safety-related components, including components of the Reactor Trip System, is performed as required by Items 3.1 and 3.2 of Generic Letter 83-28. Manufacturer, vendor and engineering recommendations provide the basis for the development of the plant's maintenance and test procedures. Following any work performed on equipment, which could affect its operability, plant procedures require that a post-maintenance operability test be performed if plant conditions allow for the retest. In the event that post-maintenance testing is perceived to degrade rather than enhance safety, appropriate changes to the test requirements will be made (see NRC SERs in References 1 and 2).

The appropriate individuals are responsible for the mechanical and electrical maintenance program and for the review and approval of the Maintenance Procedures, as required by the OQAP and applicable NADs.

The following Maintenance Procedures are typical of those prepared:

<u>Preventative Maintenance Procedures</u>

- Auxiliary Feedwater
- Auxiliary Building Ventilation
- Blowdown Treatment
- Safety Injection
- Reactor Coolant

<u>Corrective Maintenance Procedures</u>

- Service Water Dual Filter Replacement
- Gas Stripper Feed Pumps Bearing Inspection
- Containment Spray Pump Overhaul

12.4.5 INSTRUMENT AND CONTROL PROCEDURES

Instrument and Control Procedures are prepared to perform instrument and control preventative and corrective maintenance.

The appropriate individuals are responsible for the instrument and control maintenance program, and the review and approval of the Instrument and Control Procedures, as required by the OQAP and applicable NADs.

The following Instrument and Control Procedures are typical of those prepared:

- Service Water System Header 1A/1B Pressure Loops
- Component Cooling System Flow Indicators
- Reactor Coolant System Pressurizer Liquid Temperature Loop 424

12.4.6 RADIATION PROTECTION AND CHEMISTRY PROCEDURES

The appropriate individuals are responsible for the radiation protection program, the radiochemistry program, and their respective procedures, as required by the OQAP and applicable NADs.

Procedures have been prepared to identify radiochemistry methods of controlling radiation, use of instruments, methods of laboratory analysis, and records to be generated and filed.

The following procedures are typical of those prepared:

- pH Determination
- Nitrate Reagent Preparation
- Total Solids, Standardization, and Calibration

12.4.7 QUALITY PROGRAMS PROCEDURES

The Site Quality Assurance Manager is responsible for the plant Quality Programs, and is responsible to the Director, Nuclear Oversight (NMC). He is responsible for the review of plant activities affecting quality and has the Quality Programs Staff reporting to him.

The plant quality assurance programs have been reviewed by the NRC and determined to sufficiently fulfill the requirements of Items 2.1 and 2.2 of Generic Letter 83-28. These programs are specifically intended to ensure that adequate classification, control, and documentation of safety-related components and activities exist, and that vendor information for safety-related components is complete and controlled and is incorporated into plant instructions and procedures (see NRC SERs in References 3 through 6).

REFERENCES - SECTION 12.4

1. NRC Safety Evaluation Report, SA Varga (NRC) to DC Hintz (WPS), Letter No. K-85-172 dated August 12, 1985

2. NRC Safety Evaluation Report, SA Varga (NRC) to DC Hintz (WPS), Letter No. K-85-200 dated September 19, 1985

3. NRC SER, MB Fairtile (NRC) to DC Hintz (WPS), Letter No. K-87-69 dated April 7, 1987

4. NRC Safety Evaluation Report, TR Quay (NRC) to DC Hintz (WPS), Letter No. K-87-209 dated December 18, 1987

5. NRC Safety Evaluation Report, MB Fairtile (NRC) to DC Hintz (WPS), Letter No. K-86-249 dated December 23, 1986

6. NRC Safety Evaluation Report, MB Fairtile (NRC) to DC Hintz (WPS), Letter No. K-86-133 dated July 3, 1986

7. NRC Safety Evaluation Report, SA Varga (NRC) to ER Mathews (WPS), Letter No. K-82-11 dated January 26, 1982

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12.5 RECORDS AND REPORTS

Written records are kept of all plant operations, major maintenance, incidents and accidents, and radiation exposure of all personnel and are retained in accordance with the Technical Specifications and the OQAPD.

Typical of the records maintained include logbooks, inspection and test reports, personnel radiation exposures, radiation and contamination survey results, radiation work permits, results of environmental radiation surveys, records of all fuel receipts, movements, and shipments, equipment maintenance and modification files, records of radioactive solid waste shipments, training records, and composition and activity levels of liquid and gaseous releases to the environment.

Environmental qualification documentation for safety-related electrical equipment, which is required to demonstrate that equipment is capable of performing necessary safety functions under all postulated environmental conditions, is kept up-to-date in an audit able form in a central file location at the plant, pursuant to 10 CFR 50.49 (see NRC SERs in References 1, 2, and 3).

REFERENCES - SECTION 12.5

1. NRC Safety Evaluation Report, SA Varga (NRC) to CW Giesler (WPS), Letter No. K-84-199 dated September 11, 1984

2. NRC Safety Evaluation Report, SA Varga (NRC) to CW Giesler (WPS), Letter No. K-83-23 dated February 2, 1983

3. NRC Safety Evaluation Report, SA Varga (NRC) to ER Mathews (WPS), Letter No. K-81-100 dated June 18, 1981

12.6 SAFETY REVIEW AND ADMINISTRATIVE CONTROL

The organizational structure of the Wisconsin Public Service Corporation, the Nuclear Management Company, and the Kewaunee Nuclear Power Plant has been established to include sufficient managerial and supervisory personnel to provide proper administrative control and review of all aspects of operation. The Plant Manager has the responsibility and authority to operate the plant within the limits of the facility license and the Technical Specifications, and to take whatever action is necessary to assure the health and safety of the public. Within the plant organization, responsibilities for review and supervision of all activities, procedures, operations, and tests have been established. Further review and administrative control are the responsibility of the Site Vice President. In addition to these normal channels of safety responsibility, the Plant Operations Review Committee and the Joint Off-Site Review Committee have been formed.

12.6.1 PLANT OPERATIONS REVIEW COMMITTEE (PORC)

The Kewaunee Plant Operations Review Committee composition and responsibilities are discussed in the OQAPD.

12.6.2 JOINT OFF-SITE REVIEW COMMITTEE (JOSRC)

The Joint Off-Site Review Committee composition and responsibilities are discussed in the OQAPD. The JOSRC is the Kewaunee/Point Beach Off-Site Review Committee.

12.6.3 DELETED

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13.0 INITIAL TEST AND OPERATION

Note: The information presented in this section summarizes construction, pre-operational, and initial startup testing activities. As such, Section 13 is considered historical and should not be revised or updated.

Kewaunee Startup Test Program

General

The Kewaunee Startup Test Program consisted of all testing activities commencing at the completion of the construction work, and ending with the completion and acceptance of the test program prior to commercial plant operation. The startup Test Program was categorized into three "Phases":

- Phase I: Tests Prior to Initial Reactor Fueling
- Phase II: Final Plant Preparation
- Phase III: Initial Testing of the Operating Reactor

These tests were intended to demonstrate the functional performance of the safety-related systems and the overall plant. The test results provided assurance that the plant can be operated and that performance levels can be maintained in accordance with the safety requirements established in the USAR.

13.1 TESTS PRIOR TO INITIAL REACTOR FUELING (PHASE I)

The comprehensive testing program (Phase I) included construction and pre-operational tests including hot functional tests, and ended with fuel loading. Phase I testing ensured that equipment and systems performed in accordance with design criteria prior to fuel loading.

13.1.1 CONSTRUCTION TESTS

Construction tests were performed as the individual components and systems were completed. The following objectives were accomplished by these tests:

- demonstration of completeness of the equipment installation;
- provided a basis for acceptance of equipment from contributing contractors by the Architect-Engineer for the Applicant (WPS);
- established prerequisites for the subsequent pre-operational tests that were performed.

There were six distinct activities, which were carried out during the construction test program; these were:

- Installation Inspection
- Mechanical Equipment Operability Check

- Electrical Equipment Operability Check
- Instrumentation and Control Check
- Fluid System Cleaning
- Fluid System Integrity Test

The tests were performed in accordance with approved written procedures. Field and Engineering Analyses of the test results were made to verify that the systems and components had been properly installed and to recommend any corrective action, if necessary.

13.1.2 PRE-OPERATIONAL TESTS

Pre-operational tests were functional and operational tests of systems and equipment performed prior to integrated operation of the entire plant. They served two basic objectives:

a. to determine whether or not equipment and systems performed as designed;

b. to obtain initial (or baseline) equipment and system performance data.

Specific attention was given to safety-related systems, such as core cooling, since these are not used in normal operation. The pre-operational tests were designed to verify that equipment and systems would function under design conditions, and subsequent surveillance testing during the operating life of the plant continues to provide assurance that systems will fulfill their function in the unlikely event that they are required.

The contents of the test procedure, as noted in Section 13.4.3, provided sufficient information to denote complete understanding of the test requirements and the data collected permitted a thorough analysis of the system as well as a determination that the acceptance criteria have been satisfied.

The pre-operational test program in general included adjustments, calibrations, determination of pump head characteristics, heat exchanger efficiencies, and overall system capability under actual or simulated plant conditions. The test involved actual operation of the systems and equipment where possible. Where plant parameters were not available and could not be simulated, the systems were operationally tested as far as possible without these parameters; the remainder of the tests were performed when the parameters were available; for example, during hot functional testing.

Abnormal plant conditions were simulated during testing when such conditions did not endanger personnel or equipment, or contaminate clean systems. The detailed procedure took into account possible emergency or abnormal conditions involved in the test program, and appropriate safety measures were included in the procedure.

Table 13.1-1 includes a listing of actual pre-operational tests along with subcategories to show the extent and detail which was incorporated into each procedure. The requirements for each system or component determine the manner in which the procedure was prepared consistent with the construction test program and the preparation of the plant for initial fuel loading.

13.1.3 HOT FUNCTIONAL TESTS

These were pre-operational tests which were performed on major plant systems prior to initial fuel loading with the reactor coolant system at the temperature and pressure which were maintained by the operation of the reactor coolant pumps and the pressurizer heaters.

The Startup Program schedule, including the pre-operational and hot functional tests, was such that all these tests were completed prior to initial fuel loading. Table 13.1-2 contains a listing of the tests of systems and components, which were performed and accepted prior to initial fuel loading. These tests cover those systems or components, which had to be operational during fuel loading or operational on a standby basis. Additional information of pre-operational testing of specific components and systems is contained in the Inspection and Test subsections of the appropriate USAR sections. The Operational Quality Assurance Program contained supplemental information concerning procedural and organizational matters, with respect to the division of responsibility between Wisconsin Public Service Corporation, Westinghouse Electric Corporation, and Pioneer Service and Engineering Company for ensuring that the equipment and systems performed in accordance with design criteria.

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13.2 FINAL PLANT PREPARATION (PHASE II)

Phase II of the Startup Test Program included the initial fuel loading, final placement of the reactor vessel head, and post-fuel loading tests which were required prior to initial criticality.

13.2.1 CORE LOADING

The overall responsibility and direction for initial core loading was exercised by the Plant Superintendent. The overall process of initial core loading was supervised from the refueling floor of the containment structure. Prior to fuel loading, standard procedures were established for control of access by personnel; control and accountability for fuel, equipment and tools; and the maintenance of containment integrity. Westinghouse provided technical advisors to assist during the initial core loading operation.

The intended as-loaded core configuration was specified as part of the core design studies conducted well in advance of plant startup and as such was not subject to change at startup. In the event that mechanical damage was sustained during core loading operations to a fuel assembly of a type for which no spare was available on site, an alternate core loading scheme whose characteristics closely approximate those of the initially prescribed pattern would have been determined.

The Reactor Coolant System was isolated and applicable tagging and administrative procedures were used to prevent unauthorized change in the boron concentration. The boric acid tank was filled with concentrated boric acid solution and the Residual Heat Removal System was in service and available to provide moderator mixing and temperature control.

A detailed pre-loading checkoff list was followed to ensure that all requirements for the systems, equipment, and the loading operation were met. Periodically, the checkoff list was reviewed to ensure that systems and equipment continued to meet requirements of the core loading operation.

The core loading sequence followed a step-by-step procedure to insure at each loading step that:

a. Neutron sources and neutron detectors were properly located in the core during fueling. Continuous radiation monitoring was provided at the core loading stations during fuel handling and core loading operations.

b. Rod cluster control (RCC) assemblies and other components were inserted into the proper fuel assemblies prior to fuel loading.

c. Fuel assemblies with identifying marks corresponding to the correct enrichments were installed in the proper locations.

The core was assembled in the reactor vessel, which was filled with water containing enough dissolved boric acid to maintain a calculated core effective multiplication constant of 0.90 or lower. The refueling cavity was dry during initial core loading. Core moderator chemistry conditions (particularly boron concentration) was prescribed in the core loading procedure

document and was verified periodically by chemical analysis of moderator samples taken prior to and during core loading operations.

Core loading instrumentation consisted of two permanently installed source range (pulse type) nuclear channels and two temporary in-core source range channels, plus a third temporary channel which was used as a spare. The permanent channels were monitored in the Control Room by a licensed operator; the temporary channels were installed in the containment structure and monitored by engineering or operating personnel. At least one permanent channel and one temporary channel were equipped with audible count rate indicators. Both permanent channels and one temporary channel displayed neutron count rate on strip chart recorders. The temporary channels had local indication. Minimum count rates of two counts per second, attributable to core neutrons, were required on at least two of the four available nuclear channels at all times during core loading operation.

At least two artificial neutron sources were introduced into the core at appropriate specified points in the core loading program to ensure a neutron population large enough for adequate monitoring of the core.

Fuel assemblies together with inserted components (RCC assemblies, burnable poison inserts, source spider, or thimble plugging devices) were placed in the reactor vessel one at a time according to a previously established and approved sequence which was developed to provide reliable core monitoring with minimum possibility of core mechanical damage. The core loading procedure documents included a detailed tabular check sheet, which prescribed and verified the successive movements of each fuel assembly and its specified inserts from its initial position in the storage racks to its final position in the core. Multiple checks were made of component serial numbers and types at successive transfer points to guard against possible inadvertent exchanges or substitutions of components.

An initial nucleus of approximately eight fuel assemblies, the first of which contained an activated neutron source, was the minimum source-fuel nucleus, which permitted subsequent meaningful inverse count rate monitoring. This initial nucleus was determined by calculation and previous experience to be markedly sub-critical (keff $\leq$ 0.90) under the required conditions of loading.

Each subsequent fuel addition was accompanied by detailed neutron count rate monitoring to determine that the just-loaded fuel assembly did not excessively increase the count rate and that the extrapolated inverse count rate ratio was not decreasing for unexplained reasons. The results of each loading step were evaluated by Wisconsin Public Service personnel and technical advisors before the next fuel assembly was loaded.

Criteria for safe loading required that loading operations stop immediately if:

a. The neutron count rates on all responding nuclear channels doubled during any single loading step after the initial nucleus of approximately eight fuel assemblies are loaded.

b. The neutron count rate on any individual nuclear channel increased by a factor of five during any single loading step, except for cases where this increase had been predicted, such as loading the source bearing assembly on the initial step.

A containment evacuation alarm (the Hi Flux at Shutdown alarm) was coupled to the permanent source range channels, with an adjustable set point at five times the count rate to provide automatic indication of high-count rate during fuel addition.

In the event that an unacceptable increase in count rate was observed on any or all responding nuclear channels special procedures involving fuel withdrawal from the core, or detector relocation or charging of additional boric acid into the moderator would have been put into effect by licensed operational personnel of the Wisconsin Public Service Corporation, with concurrence of Westinghouse technical specialists.

Core loading procedures specified alignment of fluid systems to prevent inadvertent dilution of the reactor coolant, restricted the movement of fuel to minimize the possibility of mechanical damage, prescribed the conditions under which loading could proceed, identified chains of responsibility and authority and provided for continuous and complete fuel and core component accountability.

13.2.2 POST-LOADING TESTS

Upon completion of core loading, the reactor upper internals and the pressure vessel head were installed and additional mechanical and electrical tests were performed prior to initial criticality. The final pressure tests were conducted after filling and venting was completed.

Mechanical and electrical tests were performed on the RCC drive mechanisms. These tests included a complete operational checkout of the mechanisms. Checks were made to ensure that the RCC assembly position indicator coil stacks were connected to their respective position indicators. Similar checks were made on RCC assembly drive mechanism coils and the in-core instrumentation.

Tests were performed on the reactor trip circuits to verify manual trip operation and actual drop times were measured for each RCC assembly. By use of dummy signals, the Reactor Control and Protection System was made to produce trip signals for the various plant abnormalities that required tripping, and trip delay times were measured.

At all times that the RCC drive mechanisms were being tested, the boron concentration in the coolant-moderator was large enough that criticality could not be achieved even with all RCC assemblies fully withdrawn.

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13.3 INITIAL TESTING OF THE OPERATING REACTOR (PHASE III)

After satisfactory completion of fuel loading and final pre-critical tests, nuclear operation of the reactor began. This final phase of startup and testing included Initial Criticality, Zero Power Testing and Power Level Escalation. The purpose of these tests was to establish the operational characteristics of the plant and core, to acquire data for the proper calibration of set points, and to ensure that operation was within license requirements. A brief description of the testing is presented in the following sections. Table 13.3-1 summarizes the tests, which were performed from initial core loading to rated power.

13.3.1 INITIAL CRITICALITY

Initial criticality was established by sequentially withdrawing the shutdown and control groups of RCC assemblies from the core, leaving the last withdrawn control group inserted far enough in the core to provide effective control when criticality was achieved, and then slowly and continuously diluting the heavily borated reactor coolant until the chain reaction was self-sustaining.

Successive stages of RCC assembly group withdrawal and of boron concentration reduction were monitored by observing changes in neutron count rate as indicated by the regular source range nuclear instrumentation as functions of RCC assembly group position and, subsequently, of reactor makeup water addition to the Reactor Coolant System during dilution.

Primary safety reliance was based on count rate monitoring. The inverse count rate ratio was used as an indication of the nearness and rate of approach to criticality of the core during RCC assembly group withdrawal and during reactor coolant boron dilution. The rate of approach could have been reduced as the reactor approached extrapolated criticality to ensure that effective control was maintained at all times.

Written procedures specified alignment of fluid systems to allow controlled start and stop and adjustment of the rate at which the approach to criticality could proceed, indicated values of core conditions under which criticality was expected, specified allowed deviations in expected values, and identified chains of responsibility and authority during reactor operations.

13.3.2 ZERO POWER TESTING

A prescribed program of reactor physics measurements was undertaken to verify that the basic static and kinetic characteristics of the core were as predicted and that the values of the kinetic coefficients assumed in the safety features analysis were indeed conservative.

The measurements were made at low power and primarily at or near operating temperature and pressure. Measurements were made to permit verification of calculated values of RCC assembly group and reactivity worths, of isothermal temperature coefficient under various core conditions and of differential boron concentration reactivity worth. Relative power distribution checks were made.

Concurrent tests were conducted on the instrumentation including the source and intermediate range nuclear channels. A complete electrical and mechanical check was made on the in-core nuclear flux mapping system at the operating temperature and pressure.

Detailed procedures were prepared to specify the sequence of tests and measurements that were conducted and the conditions under which each was to be performed to ensure both safety of operation and the validity and consistency of the results obtained. If unacceptable deviations from design predictions existed, unacceptable behavior was revealed, or apparent anomalies developed, the testing would have been suspended and the situation reviewed by the Nuclear Safety Review and Audit Committee of Wisconsin Public Service Corporation to determine whether a question of safety was involved, prior to resumption of testing. Procedures also identified chains of responsibility and authority during zero power testing.

13.3.3 POWER LEVEL ESCALATION

When the operating characteristics of the reactor at zero power had been verified by the preliminary zero power tests, a program of power level escalation in successive stages brought the plant to its full rated power level.

Plant operational characteristics were closely examined at each stage and the validity of the safety features analysis was verified before escalation to the next programmed level was effected. Reactor physics measurements were made to determine the magnitudes of power coefficient of reactivity and of relative power distribution of the core as functions of power level and RCC assembly group position.

Secondary system heat balances ensured that the several indications of power level were consistent and provided bases for calibration of the power range nuclear channels. The ability of the Reactor Control and Protection System to respond effectively to signals from primary and secondary instrumentation under a variety of conditions encountered in normal operations was verified.

At prescribed power levels the response characteristics of the reactor coolant and steam systems to dynamic stimuli was evaluated. The responses of system components were measured for 10% reduction of load and recovery, 50% reduction of load and recovery, turbine trip, and trip of a single RCC assembly, as detailed in Table 13.3-1.

Adequacy of radiation shielding was verified by gamma and neutron radiation surveys inside the containment and throughout the plant site.

The sequence of tests, measurements and intervening operations was prescribed in the power escalation procedures together with specific details relating to the conduct of the several tests and measurements and chains of responsibility and authority.

13.3.4 POST-STARTUP SURVEILLANCE AND TESTING REQUIREMENTS

Post-startup surveillance and testing requirements were designed to provide assurance that essential systems, which included equipment components and instrument channels, were always capable of functioning in accordance with their original design criteria. These requirements were separated into two categories:

a. The system had to be capable of performing its function, i.e., pumps deliver at design flow and head, and instrument channels respond to initiating signals within design calibration and time responses;

b. Reliability was maintained at levels comparable to those established in the design criteria and during early plant life.

The testing requirements, as described in the Technical Specifications, established this reliability and, in addition, provided the means by which this reliability was continually reconfirmed. Verification of operation of complete systems was checked during scheduled shutdowns, such as refueling intervals, or during plant operation when means were provided to permit operation and test. Individual checks of components and instrumentation were made at more frequent intervals as outlined in the Technical Specifications.

The techniques used for the testing of instrument channels included a pre-operational calibration which confirmed values obtained during factory test programs. These reconfirmed calibration values became the reference for recalibration maintenance at refueling intervals or other scheduled shutdowns during plant life. Periodic testing, as defined in the Technical Specifications, included the insertion of a predetermined signal that tripped the channel bistable. Indication of the operation was confirmed and recorded.

Testing of components was initiated through manual actuation. If response times were important, they were measured and recorded. The capability to deliver design output was checked by instrumentation and compared against design data. Allowable discrepancies were established in the Technical Specifications. Components were operated for enough time to allow equalization of operating temperatures in bearings, seals, and motors. Checks were made on these parameters. Components were surveyed for excessive vibration. Readings were recorded.

Wisconsin Public Service Corporation verified that testing in accordance with the above-described program provided a realistic basis for determining or verifying maintenance requirements and, as such, ensured continued system capabilities, including reliability equal to that established in the original criteria.

13.3.5 PROCEDURE PREPARATION AND CHECKOUT

Normal and emergency operating procedures were prepared by WPS Operations personnel and reviewed internally by at least two members of the Operations staff. In addition, all procedures pertaining to the Nuclear Steam Supply System were reviewed by Westinghouse, and procedures pertaining to the balance-of-plant were reviewed by Pioneer Service and Engineering. Checkout

of normal operating procedures was done each time the plant was started up or shut down and when power levels were changed as required by system demand. There were other procedures which were checked when specific operations, such as fuel handling, were performed when the plant was shut down. Emergency operating procedures are checked out periodically during planned shutdown of the plant and when the plant is down for refueling and maintenance.

13.4 STARTUP TEST PROGRAM ORGANIZATION AND RESPONSIBILITIES

13.4.1 ORGANIZATION AND RESPONSIBILITY[1]

Wisconsin Public Service Corporation (WPS) had overall responsibility for supervising, directing, and ensuring that all phases of plant testing were accomplished in accordance with established criteria. Technical responsibility for each phase of testing resided with the functional group most directly concerned with the results of that phase. The WPS Manager, Power Engineering, had responsibility for ensuring that all required construction tests were accomplished and accepted. The actual performance of construction testing was within the scope of work assigned to the Architect-Engineer, Pioneer Service and Engineering Company (PS&E). The WPS Superintendent-Nuclear Power had responsibility for ensuring that all required pre-operational and startup testing was accomplished and accepted. The WPS Startup Group and Kewaunee Plant Staff were responsible to the Superintendent-Nuclear Power for planning and accomplishing the pre-operational and startup testing program. The organization for construction, pre-operational testing and startup testing is shown in Figure 13.4-1. The construction testing description and organization are shown mainly to indicate that a smooth and orderly transition of work and responsibility was carried out within the WPS organization. The current organization of the Engineering and Plant Staff can be found in Operational Quality Assurance Program Description.

13.4.2 PREOPERATIONAL AND STARTUP TESTING

The WPS Startup Group was responsible to the Superintendent Nuclear Power for planning, scheduling and coordinating and performing the pre-operational and startup testing. The Plant Test Coordinator was responsible for the supervision of personnel assigned to the testing program. All system operations in the testing program were performed by plant operators in accordance with the approved written procedures. These procedures included such items as delineation of administrative procedures and test responsibilities, test purpose, conditions, precautions, limitations and sequence of operation. Procedural changes were made only in accordance with an approved standard operating procedure that required review and approval of the changes by experienced supervisory and advisory personnel.

Startup Group

Figure 13.4-2 shows the functional organization of the Startup Group. Specific responsibilities of the Startup Group are delineated below:

a. During construction testing, the Startup Group provided assurance that all construction tests required by the USAR were conducted.

b. Monitoring construction testing by:
   - reviewing and approving construction test procedures and results;

[1] Note: Titles used herein reflect those positions in effect at time of startup testing.

- maintaining a file of completed construction test documentation as part of the "Master Test File".

c. Planning of pre-operational and startup tests to determine all testing requirements on plant systems and equipment.

d. Reviewing and approving all test procedures.

e. Determining personnel requirements for each test.

f. Scheduling to determine the sequence and dates of all tests based on the construction scheduling and test requirements.

g. Providing approved test procedures to test and operating personnel for use in conducting the tests.

h. Maintaining an up-to-date record on the progress of the test program.

i. Coordinating between Plant Test Coordinator and Plant Test Engineers on progress of test requirements.

j. Conducting meetings with plant test personnel, NSSS supplier, A-E and other technical representatives as required during the test program.

The Startup Group consisted of test engineers, each having responsibility of specific systems and reporting to the Superintendent-Nuclear Power. The Startup Group Test Coordinator had the responsibility for coordinating the efforts of all Test Engineers that had been assigned to work with him in performing the assigned tests. The Systems Test Engineer, as Startup Group Test Coordinator, had the transitional responsibility between the Construction Test Coordinator and the Plant Test Coordinator in scheduling, planning, communicating, and maintaining the progress of the startup program to assure a smooth and orderly transition of the testing program from Construction to Steam Plant Operations.

Plant Management Staff

Superintendent-Nuclear Power, responsible to the Superintendent-Steam Plants, had administrative and technical responsibility of the Kewaunee Nuclear Power Plant. The Kewaunee Plant Superintendent and the Startup Group were responsible to the Superintendent-Nuclear Power for the conduct of the test program, the smooth transition of the plant from test status to operating status and for the health and safety of the public and the plant staff.

Kewaunee Plant Superintendent was responsible for the Operations phase of the plant and formally accepted and assumed responsibility for the plant systems and equipment, which had successfully passed all construction tests.

The Assistant Plant Superintendent as the Plant Test Coordinator and reporting to the Plant Superintendent, was responsible for:

a. overall supervision of personnel performing the tests;

b. coordination with the Startup Group Test Coordinator in maintaining the progress of the test program;

c. ensuring that necessary preparations were made for scheduled tests.

The Plant Test Coordinator designated one or more supervisory personnel as "Plant Test Engineers" whose responsibility it was to assume direct supervision of assigned personnel for the specific tests to be performed.

The Shift Supervisor was responsible for the safe operation of that portion of the plant under the operational jurisdiction of the Plant Superintendent. The Shift Supervisor had direct responsibility for the operation of systems and operational equipment under test and, as such, the Plant Test Engineer coordinated his effort through the Shift Supervisor for the successful completion of the various tests.

Technical Advisors and Consultants

Technical advisors and consultants from the Nuclear Steam Supplier (Westinghouse) and the Architect-Engineer (Pioneer Service and Engineering Co.) and others, as required, provided assistance as requested by the Superintendent-Nuclear Power or his designate.

Safety Review Organization

Wisconsin Public Service Corporation had established a safety review organization, which included managerial, supervisory and technical personnel to provide proper administrative control and review of all aspects of operation. During the testing phase, the Kewaunee Plant Superintendent, had the responsibility and authority to operate the plant within the guidelines set forth by the Wisconsin Public Service Corporation Management and the Technical Specifications and to take whatever action was necessary to assure the health and safety of the public. Within the plant organization, responsibilities for review and supervision of all activities, procedures, operations and test had been established. A Plant Operations Review Committee and a Nuclear Safety Review and Audit Committee, described in Section 6.0 of the Technical Specifications, had been formed to review or audit all matters of plant operation, nuclear safety, industrial health and safety, procedures and changes to procedures, and all other matters pertaining to safe operation of the plant. Matters pertaining to nuclear safety became evident during and after the initial fuel loading.

13.4.3 PROCEDURAL MECHANICS

Preparation

Test Procedures for the Three-Phase Test Program had been prepared by the following groups:

♦ Construction Tests	Pioneer Service and Engineering[2]
♦ Pre-operational Tests	Southern Nuclear Engineering
♦ Startup Tests	Wisconsin Public Service (Startup Group and Plant Staff)

Detailed written test procedures were required for all tests of the Startup Program. The content was sufficient to ensure a complete understanding of the purpose and method of the test by the test personnel and assurance that the test could be performed without undue risk to the test personnel, equipment, and the public.

Content

Each test procedure included, if applicable, the following basic elements:

- Purpose
- References
- Summary of Procedure
- Prerequisites
- Special Equipment
- Safety Precautions
- Initial Conditions
- Procedure
- Terminal Instructions
- Acceptance Criteria
- Attachments (Data Sheets and Sketches)

Tables 13.1-1 and 13.1-2 provide the listing for some of the types of tests, which were performed on the plant.

The acceptance criteria as noted in the test procedure were derived from the detailed information, which is described in the respective sections of the USAR covering the functional requirements of systems or equipment. The acceptance criteria as written in the pre-operational test procedure were reviewed and approved by the responsible design organization. The procedure signature sheet, as it was prepared, provided for the signature of a responsible engineer in the design organization.

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[2] Now Fluor Power Services, Inc.

The Startup Test Program included the necessary tests and the sequence in which these tests were performed consistent with the construction-testing program and with the operational requirements of the plant. Some systems were required early in plant life and were, therefore, tested prior to testing of other systems. Each procedure was provided with a "Prerequisites Section" which listed all tests and conditions that had to prevail prior to the performing of the specific test. Prerequisites included, among other things, provisions for any unusual conditions, which may have existed during the test.

Review

Several reviews of each test procedure were conducted, each fulfilling a unique objective to provide assurance that the tests were planned and conducted within the guidelines of the Startup Program Manual.

A review of each procedure was made to provide assurance that it satisfies all requirements of applicable Federal Regulations, Codes, Quality Standards, and safety requirements as stated in the USAR. The procedure was considered approved when the review was complete and the procedure was signed off.

A test performance review was made of raw data as it was completed and documented to determine whether all steps were performed and all data properly recorded. It was the responsibility of the Plant Test Coordinator to perform this review prior to releasing the test results for complete review and analysis.

A thorough review and analysis was made of the data recorded for each test procedure to determine whether or not all test requirements and the acceptance criteria had been met. The results of the analysis were recorded for each procedure, which served as the documentation that the system or equipment had been tested, accepted and ready for the next phase of testing or operation.

Approvals

Each test procedure was accompanied by an approval signature sheet, signed and dated by the individuals responsible for the review. The same sheet was also used for the signatures of the individuals responsible for supervising the test and approving the test results.

The responsibility for approving the procedures, test data, and the results was placed upon personnel in the following disciplines: Technical, Design, Test, Operations, and Quality Assurance. The Quality Assurance engineer was responsible for the review of the test procedure to determine that the test would be performed within the guidelines of the Operations Quality Assurance Manual, Applicable Federal Regulations, Codes, and Standards. The test results were audited by him or his designate.

Changes to Approved Procedures

Requests for changes to approved test procedures were made to the document originator through the responsible Startup Group Test Engineer. Normally, all changes to the approved test procedures were reviewed and approved prior to the performance of the test by the same individuals or groups that reviewed and approved the previous issue.

In special instances, when the normal process of initiating a change to an approved procedure as noted above would result in a significant delay in the schedule of the test program, the procedure followed is described below.

The test procedure requiring a change was provided with a special form "Test Procedure Field Revision". The required change was noted on the form and approved by the responsible Plant Test Engineer and the responsible Startup Group Test Engineer. Once approved, this form became a part of the test procedure. During the test results analysis, the test procedure revision was reviewed to determine whether the change had any effect on the validity of the test and appropriate action recommended, if necessary.

13.4.4 TECHNICAL ASSISTANCE - TEST PROGRAM

The Kewaunee Nuclear Power Plant Startup Group was responsible for the planning and execution of the pre-operational and startup test program. The six engineers had worked very closely with Southern Nuclear Engineering (SNE) personnel in the preparation of the pre-operational test procedures. The Startup Group's expertise covered core physics, chemistry and chemical engineering, electrical engineering, instrumentation and control, and nuclear plant operations. The Startup Group had developed a schedule for the test program and scheduled the requirement for the technical support personnel. Personnel from the General Steam Plant Engineering Group to assist operations personnel in the acceptance and testing program of the balance of plant systems. Additional trained personnel, for example instrument technicians, were used to assist on an as required basis. To further assist the Startup Group, technical consultants from the following companies were available during the test program.

Westinghouse

The Westinghouse site technical assistance that was provided for startup testing was dependent upon the tests being performed and the level of testing activity. Westinghouse Nuclear provided WPS a Systems/Startup Engineer on site to follow major systems and hot functional testing.

In addition, support personnel were available to the Systems/Startup Engineer. The support engineers covered mechanical, process control, and nuclear instrumentation engineering. To augment these engineers, Westinghouse provided specialists as required in areas such as transient analysis, chemistry, thermal and hydraulics, and fuel. A reactor physicist was on site to assist and evaluate reactor core performance from fuel loading through the zero power and power ascension testing. Westinghouse personnel reviewed the NSSS procedures prior to their implementation and assisted in on-site review of test results and, if required, further evaluation of the results by the home office staff was available. Westinghouse Instrument Services

Company had personnel on site and additional personnel were available as required to assist in the further calibration and testing of instrumentation systems.

Pioneer Service and Engineering

Pioneer had five technical personnel on site assisting in the test program. These were augmented, on an as required basis, with five technical specialists. The five specialists had responsibilities during design and construction and covered such disciplines as structural and hydraulic engineering, mechanical engineering, steam plant and turbine auxiliaries engineering, electrical engineering, and process control engineering. They were available to the Startup Group to assist in interpreting procedures, test results and any problems that could have arisen, during the test program.

Southern Nuclear Engineering

SNE had prepared the pre-operational test procedures for the Kewaunee Nuclear Power Plant. The procedures were prepared in close cooperation with the WPS Startup Group requiring review by both Pioneer and Westinghouse. The SNE engineers developing the procedures had experience in preparing procedures, had been involved in testing programs, or had been licensed operators on other nuclear power plants. SNE had seven technical consultants available for support during the test program. The disciplines which these support personnel covered were power plant, electrical, core design, control rod drive, process control, nuclear instrumentation, and reactor protection system engineering.

SNE had been involved with the Kewaunee Project for over two years and had become thoroughly familiar with all aspects of the plant design. In addition, SNE had available other technical staff members who provided assistance as necessary.

Foxboro Instrument Company

Foxboro had provided the bulk of the process instrumentation for the Kewaunee Plant. They provided at least two engineers to assist in the further calibration of the instrument systems and to train WPS personnel. They were available to perform trouble shooting of instrument systems during the test program. Foxboro had provided instrumentation systems for many nuclear power plants and, therefore, was well qualified in understanding the needs of the utility during the startup test program.

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13.5 CORRECTIVE ACTION

Repair, modification, and adjustment of equipment necessitated by construction testing were performed by the construction contractors under the supervision of the Construction Manager (PS&E). This work was done under the existing Quality Assurance guidelines in effect during construction.

Repair, modification and adjustment of equipment under the operational jurisdiction of WPS were performed by WPS maintenance and operational personnel. The guidelines for work of this nature were covered under the Operational Quality Assurance Program. The QA procedures employed had been prepared, approved, and authorized for use by WPS Management.

Any deficiencies, which were discovered as a result of testing, were noted by the responsible Test Engineer on a Deficiency Report. The Plant Test Coordinator maintained a deficiency file for all operational equipment. This file contained deficiency reports resulting from construction, pre-operational and startup testing. These reports remained active until the deficiency had been corrected. Items requiring resolution by the contractor, Architect-Engineer, Nuclear Steam Supplier, or Turbine-Generator Supplier, were brought to his attention immediately. Notification of correction of the deficiency was made to the Plant Test Coordinator so that the correction could be verified and the Deficiency Report removed from the active file.

Temporary Changes

Temporary equipment changes made for testing, such as electrical jumpers, terminal disconnections, temporary piping connections, abnormal valve or electrical lineups, abnormal instrument settings and removal of normal interlocks, were detailed as precautions in the written test procedure. They were further noted in the Terminal Instructions portion of the Pre-operational Test Procedure to make certain the system or equipment was returned to its normal condition following the test.

Administrative procedures requiring the signatures of numerous responsible staff personnel provided assurance that temporary actions taken during or prior to the performance of a test were corrected, noted and documented. In addition, a "Temporary Change Log" was maintained by the Shift Supervisor which listed all temporary changes which were made during the performance of a test and not restored to the original condition at the conclusion of the test; or changes which were not removed because the test procedures did not call for removal of the temporary changes. All temporary changes which were specifically spelled out in the test procedure and restored to their original condition during the performance of the test were not logged in the "Temporary Change Log" since the procedure already documented this fact.

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13.6 DOCUMENTATION

Documentation commenced with the written test procedure. Each procedure was accompanied with a signature sheet, which denoted that the procedure had been prepared, reviewed, and approved by responsible personnel. It further noted that the test was supervised and test results approved by responsible staff and design personnel as required.

Additional documentation consisted of the following items:

- Test Procedure Field Revision
- Equipment Operational Release (used after Construction Testing)
- Deficiency Reports
- Deficiency File
- Temporary Change Log
- Quality Assurance Surveillance and Audit Reports

The above have been discussed previously with the exception of (b), Equipment Operational Release covered release of equipment or systems following the Construction Testing. Boundary tag requirements were used during construction. The tags denoted condition of the equipment, whether it was on "HOLD', "OPERATIONAL", (authorizing only WPS Kewaunee Nuclear Power Plant Operations personnel the right to operate it), or "SYSTEM UNDER TEST", which was used in construction testing. The test data, results, and all related reports were maintained on file in the site Master Test File.

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13.7 SCHEDULING

13.7.1 CONSTRUCTION TESTS

Construction tests were scheduled in such a manner as to permit testing any major component or system as soon as construction had been completed to permit evaluation of the test results to determine installation, contractual, and procurement specification agreement. In some instances, the results of the construction test were adequate to permit use of the component or system as a service system during the remaining construction and testing phase.

13.7.2 PREOPERATIONAL TESTS

The pre-operational tests were scheduled to follow construction tests, so as to minimize the time between system testing and initial fuel loading. These included the hot functional tests. The pre-operational tests demonstrated the functional performance of all safety-related systems as well as tests on systems, which were required for plant operation but were not safety related. During the pre-operational testing phase, the systems were tested under the conditions of the plant existing at the time of test. In many instances, certain tests were delayed, as noted in Table 13.1-2, until proper conditions of pressure and temperature were achieved. In still other instances, complete system performance was not checked out until the plant was at some power level and the systems operational. These delayed tests are noted in Table 13.3-1.

WPS had set a time limit of six months as a maximum time between performance of the test and operational use of the equipment or system. If a system or piece of equipment, which had been tested, sat idle for a period of six months or longer, consideration was given to complete or partial re-testing, prior to that system being placed into service.

13.7.3 POST CORE LOADING AND INITIAL TESTS

Initial core loading followed the hot functional tests. Following the core loading, the initial testing of the operating reactor, as discussed in Section 13.3, commenced. The tests were scheduled to allow sufficient time to proceed from initial criticality through zero power to power level escalation.

Table 13.3-1 summarized the tests, which were performed. The power plateaus at which various tests were performed are from hot zero power to 100% full power, with intermediate power levels between 10% and 100% used to verify performance of specific systems and to determine whether the acceptance criteria had been met. The tests had been designed to permit a smooth transition from initial core loading to escalation in power, enabling Steam Plant Operations to perform various tests, to obtain data which would further determine the plant capability as far as safety-related requirements were concerned. The test results were analyzed to ascertain that the plant was capable of responding to accident conditions and transients, as described in the USAR, and further provided with reasonable assurance that the design bases had been met.

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TABLE 13.1-1
KEWAUNEE - PREOPERATIONAL TESTS (TYPICAL)

TITLE AND SUB TESTS

PLANT GENERAL

Set Point Verification
Composite Hot Functional Tests

STATION INSTRUMENT AIR SYSTEM

Functional Test
    Loss of Instrument Air Test

CONDENSATE SYSTEM

Functional Test

CIRCULATING WATER SYSTEM

Functional Test

FEEDWATER SYSTEM

Functional Test

MAIN STEAM AND STEAM DUMP SYSTEM

Main Steam Safety Valve Test
Steam Dump Control
Hot Functional Test

BLOWDOWN TREATMENT

Functional Test

FIRE PROTECTION SYSTEM

Functional Test
Complete Component Test

AIR REMOVAL SYSTEM

Functional Test

TABLE 13.1-1
KEWAUNEE - PREOPERATIONAL TESTS (TYPICAL)

TITLE AND SUB TESTS

DIESEL GENERATOR SYSTEM

Functional Test
- Mechanical and Electrical
- Automatic and Manual

AUXILIARY BUILDING SPECIAL VENTILATION

Initial Operation
Functional Test

REACTOR BUILDING VENTILATION SYSTEM

Functional Test

TURBINE OIL PURIFICATION SYSTEM

Functional Test

SPENT FUEL POOL COOLING SYSTEM

Functional Test

INTERNAL CONTAINMENT SPRAY SYSTEM

Functional Test - Pump and Nozzle Verification Tests

SHIELD BUILDING VENTILATION SYSTEM

Functional Test

CONTROL ROOM AIR CONDITIONING

Functional Test
Emergency Operation

CHEMICAL INJECTION

Functional Test

TABLE 13.1-1
KEWAUNEE - PREOPERATIONAL TESTS (TYPICAL)

TITLE AND SUB TESTS

COMPONENT COOLING SYSTEM

Functional Test

WASTE DISPOSAL SYSTEMS

Functional Tests
- Liquid System
- Gaseous System
- Solid System

SAFETY INJECTION SYSTEM

Functional Test

RESIDUAL HEAT REMOVAL SYSTEM

Functional Test
- Normal and Emergency Power

CHEMICAL AND VOLUME CONTROL SYSTEM

Functional Test
- Makeup Control - Blending - Mixing
- Charging Pump Control
- Boron Recycle - Heat Tracing, etc.

REACTOR COOLANT SYSTEM

Filling, Venting, and Hydro
- Hot Functional Test - Heat-Up
- Hot Functional Test - At Temperature
- Hot Functional Test - Cool Down
- Hot Functional Test - Thermal Expansion
- Hot Functional Test - Vibration
- Hot Functional Test - RTD Calibration
- RCS Heat Loss Measurement

REACTOR COOLANT PUMP

Functional Test

TABLE 13.1-1
KEWAUNEE - PREOPERATIONAL TESTS (TYPICAL)

TITLE AND SUB TESTS

PRESSURIZER

Pressurizer Relief Valve Test
Pressurizer Safety Valve Test
Pressurizer Pressure Control
Pressurizer Level Control

PRESSURIZER RELIEF TANK

Functional Test

STEAM GENERATOR

Hydro Test, Primary and Secondary Side
Steam Generator Level Control

PRIMARY SAMPLING SYSTEM

Functional Test

D-C SUPPLY AND DISTRIBUTION SYSTEM

Functional Tests

4160 V. SUPPLY AND DISTRIBUTION SYSTEM

Functional Tests
- Circuit Breaker Test
- Integrated Logic
- Synchronizing and Energization
- Emergency A-C Power Test

480 V. SUPPLY AND DISTRIBUTION SYSTEM

Functional Tests
- Circuit Breaker Test
- Integrated Logic
- Synchronizing and Energization
- Emergency A-C Power Test

TABLE 13.1-1
KEWAUNEE - PREOPERATIONAL TESTS (TYPICAL)

TITLE AND SUB TESTS

LIGHTING SYSTEM

Emergency Lighting

ELECTRICAL GENERATION SYSTEM

Functional Test

COMMUNICATION SYSTEM

Functional Test

RADIATION MONITORING SYSTEM

Area Monitoring Functional Test
Process Monitoring Functional Test

REACTOR CONTROL AND PROTECTION SYSTEM

Functional Tests

NUCLEAR INSTRUMENTATION SYSTEM

Source Range Functional Test
Intermediate Range Functional Test
Power Range Functional Test

CONTROL ROD DRIVE SYSTEM

Functional Test
- Rod Control System
- Rod Drop Test
- Position Indication Test

IN-CORE INSTRUMENTATION SYSTEM

Functional Test
- In-Core Set Point Determination
- In-Core Thermocouple Functional Test

TABLE 13.1-1
KEWAUNEE - PREOPERATIONAL TESTS (TYPICAL)

TITLE AND SUB TESTS

MISCELLANEOUS GAS SYSTEM

Hydrogen Supply Functional Test
Nitrogen Supply Functional Test

SEQUENTIAL EVENT RECORDER AND ANNUNCIATORS

SER Functional Test

FUEL HANDLING SYSTEM

Fuel Handling and Storage Functional Test
Preparation for Initial Core Loading
Fuel Transfer System Functional Test
Manipulator Crane Functional Test
Spent Fuel Elevator Functional Test

AUXILIARY FEEDWATER SYSTEM

Functional Test

LEAK DETECTION SYSTEM

Functional Test

CONTAINMENT VESSEL

Functional Test
- Leak Rate
- Integrity

Penetration Pressure Test
Containment Isolation Test

TABLE 13.1-2
OBJECTIVES OF SYSTEM TEST PRIOR TO INITIAL REACTOR FUELING

SYSTEM TESTS	TEST OBJECTIVES
1. Electrical System	To ensure continuity, circuit integrity, and the correct and reliable functioning of electrical apparatus. Electrical tests were performed on transformers, switchgear, turbine-generator, motors, cables, control circuits, excitation switchgear, d-c systems, annunciator systems, lighting distribution switchboards, and miscellaneous equipment. Special attention was directed to the following tests: (a) High voltage switchgear breaker interlock test. (b) Plant loss of voltage auto-transfer test. (c) Emergency power transfer test. (d) Tests of protective devices. (e) Equipment automatic-start tests. (f) Exciter check for proper voltage build-up.
2. Communication System	Verified proper communication between all local stations for interconnection to commercial phone service and load dispatch center, and to balance and adjust amplifiers and speakers.
3. Service Water System	Verified the design head-capacity characteristics of the pumps, that the system supplies design flow rate through heat exchangers, and the specified requirements when operated in the safety features mode. (This system was used during construction. Its extensive use was accepted as a pre-operational test.)
4. Fire Protection System	Verified proper operation of the system by ensuring the design specifications were met for the fire pumps, verified that automatic start functions operate as designed, and verified that level and pressure controls met specifications.
5. Instrument Air System	Verified leaktightness of the system, proper operation of all compressors, the manual and automatic operation of controls at design setpoints, design air-dryer cycle time and moisture content of discharge air, and proper air pressure to each controller served by the system.

TABLE 13.1-2
OBJECTIVES OF SYSTEM TEST PRIOR TO INITIAL REACTOR FUELING

SYSTEM TESTS	TEST OBJECTIVES
6. Reactor Coolant System Cleaning	Flushed and cleaned the reactor coolant and related systems to obtain the degree of cleanliness required for the intended service. Provisions to maintain cleanliness and protection from contaminated sources were made after system cleaning and acceptance. After systems were flushed clean of particulate matter, the cleanliness of the system was maintained. Coolant was analyzed for chloride content, solids, pH, and conductivity. Oxygen content was analyzed and brought to specifications prior to exceeding 250°F.
7. Ventilation System	Verified proper operability of fans, controls, and other components of the Reactor Building Ventilation System, Auxiliary Building Special Ventilation Systems and Shield Building Ventilation System.
8. Condensate and Feedwater System	To verify valve and control operability and setpoints, an inspection was made. Functional testing is performed when the Main Steam System is available.
9. Auxiliary Coolant	Verified component cooling flow to all components, and verified proper operation of instrumentation, controllers, and alarms. Specifically, each of the three systems; i.e., Component Cooling System, Residual Heat Removal System, and Spent Fuel Pool Cooling System, were tested to ensure: (a) All manual and/or remotely operated valves were operable manually and/or remotely. (b) All pumps performed their design functions satisfactorily. (c) All temperature, flow, level, and pressure controllers functioned to control at the required setpoint when supplied with appropriate signals. (d) All temperature, flow, level, and pressure signals provided alarms when the required alarm set-point when the reset-point was reached. (e) Design flow rates were established through the principal heat exchangers.

TABLE 13.1-2 OBJECTIVES OF SYSTEM TEST PRIOR TO INITIAL REACTOR FUELING	
SYSTEM TESTS	**TEST OBJECTIVES**
10. Chemical and Volume Control System	Verified that the Chemical and Volume Control System functioned as specified in the system description. More specifically that: (a) All manual and/or remotely operated valves were operable manually and/or remotely. (b) All pumps performed to specifications. (c) All temperature, flow, level, and pressure controllers functioned to control at the required setpoint when supplied with appropriate signal(s). (d) All temperature, flow, level, and pressure signals provided alarms when the required alarm setpoint was reached, and clear when the reset point was reached. (e) The reactor makeup control regulated blending, dilution, and boration as designed. (f) The design seal water flow rates were attainable at each reactor coolant pump. (g) Chemical Addition Subsystem functioned as specified.
11. Safety Injection System	Verified response to control signals and sequencing of the pumps, valves, and controllers of this system as specified in the system description and checked the time required to actuate the system after a safety injection signal was received. More specifically that: (a) All manual and/or remotely operated valves were operable manually and/or remotely. (b) Each pair of valves installed for redundant flow paths operated as designed. (c) All pumps performed their design functions satisfactorily. (d) The proper sequencing of valves and pumps occurred on initiation of a safety injection signal. (e) The fail position on loss of power for each remotely operated valve was as specified. (f) Valves requiring signals, such as high containment pressure, operated when supplied with these signals. (g) All level and pressure instruments were set at the specified points and provided alarm and reset at the required location(s).

TABLE 13.1-2
OBJECTIVES OF SYSTEM TEST PRIOR TO INITIAL REACTOR FUELING

SYSTEM TESTS	TEST OBJECTIVES
12. Containment Vessel	Verified response to control signals and sequencing Internal Spray System of the pumps, valves, and controllers as specified in the system description and checked the time required to actuate the system after a containment high-high pressure signal was received. More specifically, refer to the test objectives for Safety Injection System given above.
13. Fuel Handling Systems*	Showed that the system design was capable of providing a safe and effective means of transporting and handling fuel from the time it reaches the plant until it leaves the plant. In particular, the tests were designed to verify that: (a) The major structures required for refueling, such as the reactor refueling cavity, new fuel and spent fuel storage, and decontamination facilities, were in accordance with the design intent. (b) The major equipment required for refueling such as the manipulator crane, fuel handling tools, spent fuel transfer system, operated in accordance with the design specifications. (c) All auxiliary equipment and instrumentation functioned properly.
14. Radiation Monitoring System	Verified the calibration, operability, and alarm setpoints of all area radiation monitors, air particular monitors, gas monitors, and liquid monitors which were included in the Process Radiation Monitoring System and the Area Radiation Monitoring System.
15. Reactor Control and Protection System	Verified calibration, operability, trip and alarm Settings of the Reactor Control and Protection System and tested its operability in conjunction with other systems. As an example, the Nuclear Instrumentation System tests are detailed below.

TABLE 13.1-2
OBJECTIVES OF SYSTEM TEST PRIOR TO INITIAL REACTOR FUELING

SYSTEM TESTS	TEST OBJECTIVES
16. Nuclear Instrumentation System	Ensured that the instrumentation system was capable of monitoring the reactor leakage neutron flux from source range through full power and that protective functions were operating properly. In particular, the tests were designed to verify that: (a) All system equipment, cabling, and interconnections were properly installed. (b) The source range instrumentation operated properly, and that the source range (high flux level reactor trip) as well as alarm features and audible count rate operated properly. (c) The intermediate range instrumentation operated properly, the reactor protective and control features such as high level reactor trip and high level rod stop signals operated properly, and the permissive signals for blocking source range trip and source range "high voltage off" operated properly. (d) The power range instrumentation operated properly, the protective features such as the overpower trips, permissive and dropped-RCCA functions operated with the required redundancy and separation through the associated logic matrices; and the nuclear power signals to other systems were available and operating properly. (e) All auxiliary equipment such as the startup rate channel recorders and indicators operated properly. (f) All instruments were properly calibrated and all setpoints and alarms were properly adjusted.
17. Waste Disposal System	Verified satisfactory flow characteristics through the equipment, demonstrated satisfactory performance of pumps and instruments, checked for leaktightness of piping and equipment, and verified proper operation of monitors, alarms, and controls. (a) All equipment in the waste disposal system, which may be required during fuel loading was checked for proper operation and was in operation or on standby. (b) All alarms were operable at required locations. (c) Pumps, valves, and controllers required in (a) above were operable.

TABLE 13.1-2
OBJECTIVES OF SYSTEM TEST PRIOR TO INITIAL REACTOR FUELING

SYSTEM TESTS	TEST OBJECTIVES
18. Sampling System	Verified that a specified quantity of representative fluid could be obtained safely from each sampling point. In particular, the tests were designed to verify that: (a) All system piping and components were properly installed. (b) All remotely and manually operated valving operated in accordance with the design specifications. (c) All sample containers and quick-disconnect couplings functioned properly.
19. Emergency Power System	Demonstrated that the system was capable of providing power for operation of vital equipment under failure of normal power supply. In particular, the tests were designed to verify that: (a) All system components were properly installed. (b) Each diesel generator functioned according to the design specification under emergency conditions. (c) The diesel generators were capable of supplying the power to vital equipment as required under emergency conditions. (d) All redundant features of the system functioned according to the design intent.

TABLE 13.1-2 OBJECTIVES OF SYSTEM TEST PRIOR TO INITIAL REACTOR FUELING	
SYSTEM TESTS	**TEST OBJECTIVES**
20. Hot Functional Tests	Using pump heat and pressurizer heaters, the Reactor Coolant System was tested to check heatup and cooldown procedures demonstrating satisfactory performance of components that were exposed to the reactor coolant temperature; verified proper operation of instrumentation, controllers and alarms, and provided design operating conditions for checkout of auxiliary systems. The Chemical and Volume Control System was tested to determine that water can be charged at rated flow against normal Reactor Coolant System pressure; checked letdown flow against design rate for each pressure reduction station; determined the response of the system to changes in pressurizer level; checked operation of the reactor makeup control; checked operation of the excess letdown and seal water flow path; and verified proper operation of instrumentation, controls and alarms. The Sampling System was tested to determine that a specified quantity of representative fluid could be obtained safely and at design conditions from each sampling point. The Auxiliary Cooling System was tested to evaluate its ability to remove heat from systems containing radioactive fluid and other special equipment; verified component cooling flow to all components; and verified proper operation of instrumentation, controllers and alarms. The ventilation systems were tested to adjust proper flow characteristics of ducts and equipment; to demonstrate satisfactory performance of fans, filters, and coolers; and to verify proper operation of instruments and alarms.
21. Rod Drive Electrical Checkout	Checked the system's electrical response to test signals and verified control functions. Prior to fuel loading and after the position indication coils were installed, a calibration and complete operational check was performed.

TABLE 13.1-2
OBJECTIVES OF SYSTEM TEST PRIOR TO INITIAL REACTOR FUELING

SYSTEM TESTS	TEST OBJECTIVES
22. Primary and Secondary System	Tested and set pressurizer and secondary system Safety and Relief Valves safety and relief valves to check lifting pressure.
23. Cold Hydrostatic Tests	Verified the integrity and leaktightness of the Reactor Coolant System and related systems with the performance of a hydrostatic test at the specified test pressure.
24. Containment Pressure Test	Verified the structural integrity and leaktightness of the containment.
25. Nitrogen System	Verified system integrity, valve operability, regulating and reducing station performance and the ability to supply nitrogen to interconnecting systems as required.
* Tests were conducted with a dummy fuel element.	

TABLE 13.3-1
PHASE III TESTING SUMMARY

TEST	CONDITIONS	OBJECTIVES	ACCEPTANCE CRITERIA
RCC Assembly Drop Tests	(a) Cold, shutdown (b) Hot, shutdown	Measured the drop time of RCC assemblies under full-flow and no-flow	Droptime less than value assumed in safety analysis conditions
Thermocouple/RTD Inter-calibration	Various temperatures during system heatup at zero power and/or cooldown	Determined in-place isothermal correction constants for all core exit thermocouples and reactor coolant RTDs	Sensors showing excessive deviations from average were removed from service
Nuclear Design Check Tests	Normal control group configurations at hot zero power	Verified that nuclear design predictions for endpoint boron concentration and isothermal temperature coefficient were valid, to verify power distribution	Technical Specification limiting values
Control Group Calibration	All RCC assembly groups at hot zero power	Verified that nuclear design predictions for RCC assembly group differential worths were valid	Technical Specification limiting values
Power Coefficient Measurement	10% to 100% of rated power	Verified that nuclear design predictions for differential power coefficients were valid	SAR Criteria Applicable
Automatic Control System Checkout	Approximately 20% of rated power	Verified control system response characteristics for the: (a) Steam generator level (b) RCC assembly automatic control system (c) Turbine control system	Not Safety Related
Power Range Instrumentation Calibration	During static and/or transient conditions at the following nominal percentages of rated power: 30% 50% 75% 90% 100%	Verified all power range instrumentation consisting of power range nuclear channels, in-core exit thermocouple system, and reactor coolant RTDs were responsive to changes in reactor power distribution and inter-calibrated the several systems	Calibrate instruments to agree with thermal power measurements

TABLE 13.3-1
PHASE III TESTING SUMMARY

TEST	CONDITIONS	OBJECTIVES	ACCEPTANCE CRITERIA
Load Swing Test	± 10% steps at the following nominal percentages of rated power: 30% 75%	Verified control system's performance as evidenced by plant parameter variations	Plant parameter variations were within acceptable limits
Load Rejection Test	Load rejection from the following nominal percentages of rated power: 30% 50%	Verified control system's performance as evidenced by plant parameter variations	Plant parameter variations were within acceptable limits
Pressurizer Spray Effectiveness Test	Hot, shutdown	Verified that pressurizer pressure was reduced at the required rate by pressurizer spray actuation	Acceptable rate of pressure decrease
Minimum Shutdown Verification	Hot, zero power	Verified the nuclear design prediction of the minimum shutdown boron concentration with one "stuck" RCC assembly	Verify "stuck" RCC assembly shutdown criteria
Static RCC Assembly Test	50% of rated power	Verified that a single RCC assembly inserted fully or part way below control bank resulted in hot channel factors below design values	USAR limiting values for dropped RCCA analysis
Step Load Reduction Test	Reduction from 75% to 25% of rated power, 50% reduction from 100% of rated power	Verified control system's performance as evidenced by plant parameter variations	Plant parameter variations were within acceptable limits
Part-Length Group Operational Maneuvering	50% to 80% of rated power	Verified that the part-length RCC assembly maneuvering scheme was effective in controlling and suppressing axial power distribution transients	Operability of plant under transient conditions without actuating runback or trip

TABLE 13.3-1
PHASE III TESTING SUMMARY

TEST	CONDITIONS	OBJECTIVES	ACCEPTANCE CRITERIA
Dynamic RCC Assembly Drop Test	< 50% of rated power	Verified automatic detection of dropped RCC assembly	Required power reduction and RCC assembly withdrawal block accomplishment
Turbine Generator Startup Tests	Pre- and Post-Synchronization	Verified that the turbine generator unit and associated controls and trips were in good working order and ready for service	Successful completion of all mechanical, electrical, and control functional checks
Turbine Generator Trip	> 30% of rated power	Verified normal trouble free performance of the turbine generator at low power	Performance within manufacturers' limitations
Acceptance Run	100 Hours at rated power	Verified reliable steady-state full power capability	100 Hours reliable equilibrium operation at full power

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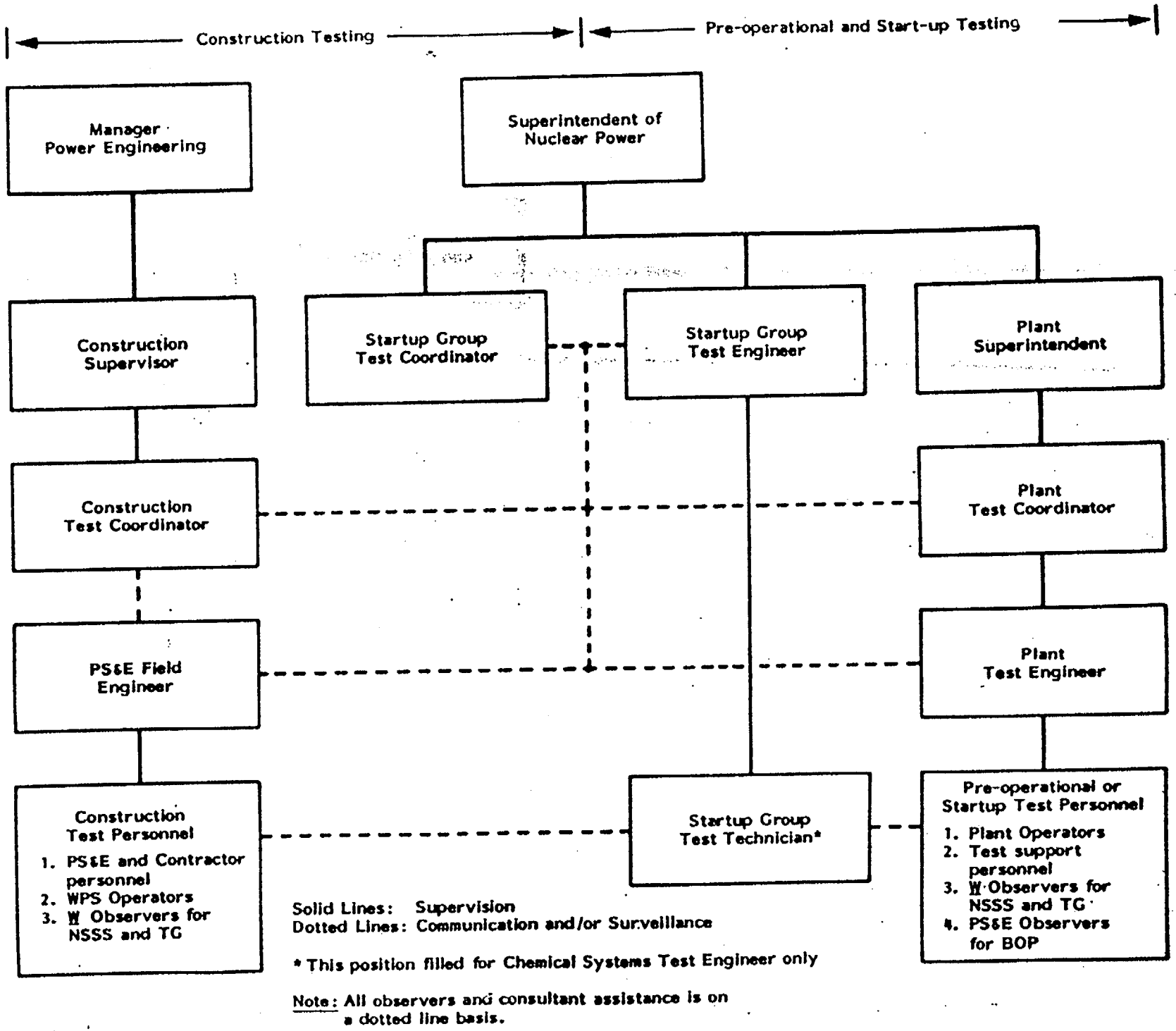
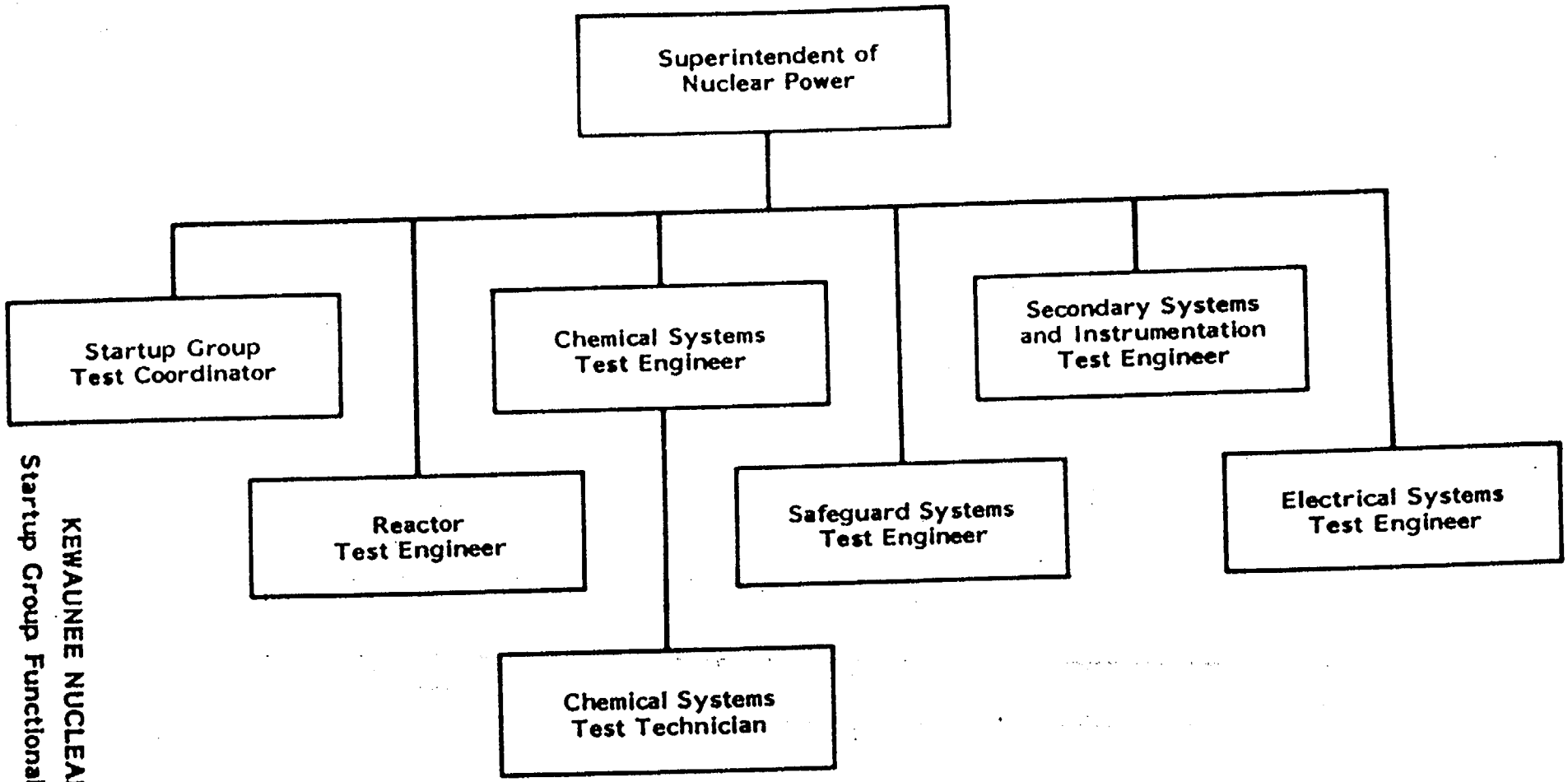


KEWAUNEE NUCLEAR PLANT
Organization - Test Program

Figure 13.4-1



KEWAUNEE NUCLEAR PLANT

Startup Group Functional Organization

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14.0 SAFETY ANALYSIS

SAFETY ANALYSIS OVERVIEW

In this section the safety aspects of the plant are evaluated to demonstrate that the plant can be operated safely and that radiological consequences from postulated accidents do not exceed the guidelines of 10 CFR 100.

The American Nuclear Society (ANS), Reference 1, has classified plant conditions into four categories in accordance with the anticipated frequency of occurrence and potential radiological consequences to the public. The four categories are as follows:

- Condition I: Normal Operation and Operational Transients
- Condition II: Incidents of Moderate Frequency
- Condition III: Infrequent Incidents
- Condition IV: Limiting Faults

A description of each category including design requirements, acceptance criteria, and the applicable design basis transient events is provided below:

Condition I: Normal Operation and Operational Transients

Definition

Condition I occurrences are operations that are expected frequently or regularly in the course of power operation, refueling, maintenance, or maneuvering of the plant.

Design Requirements

Condition I occurrences shall be accommodated with margin between any plant parameter and the value of that parameter which would require either automatic or manual protective action.

Events

Normal Operation (Base Load and Load Follow)

Acceptance Criteria

- No Clad Damage/Fuel Melting
- Reactor Coolant System Pressure < Design Limits
- Main Steam System Pressure < Design Limits
- Containment Pressure and Temperature < Design Limits

Condition II: Incidents of Moderate Frequency

Definition

Condition II occurrences include incidents, any one of which may occur during a calendar year for a particular plant.

Design Requirements

Condition II incidents shall be accommodated with, at most, a shutdown of the reactor with the plant capable of returning to operation after corrective action. Any release of radioactive materials in effluents to unrestricted areas shall be in conformance with Paragraph 20.1 of 10 CFR Part 20, "Standards for Protection Against Radiation".

By itself, a Condition II incident cannot generate a more serious incident of the Condition III or IV type without other incidents occurring independently. A single Condition II incident shall not cause consequential loss of function of any barrier to the escape of radioactive products. (No fuel rod failure or RCS overpressurization).

Transient Events

- Uncontrolled RCCA Withdrawal From Sub-critical
- Uncontrolled RCCA Withdrawal at Power
- RCCA Misalignment (Dropped/Static)
- Chemical and Volume Control System Malfunction
- Startup of Inactive Reactor Coolant Loop
- Feedwater System Malfunction
- Excessive Load Increase
- Partial Loss of Reactor Coolant Flow
- Loss of External Load
- Loss of Normal Feedwater
- Loss of AC Power to Plant Auxiliaries

Acceptance Criteria

- Reactor Coolant System Pressure < 110% of Design (2750 psia)
- MDNBR > MDNBR Limit
- Fuel Centerline Temp < 4700°F
- Dose Consequences < 10CFR20
- Main Steam System Pressure < 110% of Design (1210 psia)
- Containment Pressure and Temperature < Design Limits

Condition III: Infrequent Incidents

Definition

Condition III occurrences include incidents, any one of which may occur during the lifetime of a particular plant.

Design Requirements

Condition III incidents shall not cause more than a small fraction of the fuel elements in the reactor to be damaged, although sufficient fuel element damage might occur to preclude resumption of operation for a considerable outage time.

The release of radioactive material due to Condition III incidents may exceed guidelines of 10 CFR Part 20, "Standards for Protection Against Radiation", but shall not be sufficient to interrupt or restrict public use of those areas beyond the exclusion radius.

A Condition III incident shall not, by itself, generate a Condition IV fault or result in a consequential loss of function of the Reactor Coolant System or reactor containment barriers.

Transient Events

- Small LOCA
- Small Steam Line Break
- Complete Loss of Reactor Coolant Flow
- Single RCCA Withdrawal at Power
- Fuel Assembly Misloading
- Volume Control Tank Rupture

Acceptance Criteria

Most incidents use Condition II criteria, which are more limiting than the Condition III criteria. If these are not satisfied, the following criteria are applied:

- MDNBR < MDNBR Limit - Small Fraction of Fuel Rods (< 5%)
- Dose Consequences < 10% of 10CFR100
- RCS Pressure < 2900 psia
- Containment Pressure and Temperature < Design Limits

Condition IV: Limiting Faults

Definition

Condition IV occurrences are faults that are not expected to occur but are postulated because their consequences would include the potential for the release of significant amounts of

radioactive material. Condition IV faults are the most drastic, which must be designed against, and thus represent the limiting design cases.

Design Requirements

Condition IV faults shall not cause a release of radioactive material that results in an undue risk to public health and safety exceeding the guidelines of 10 CFR 100, "Reactor Site Criteria". A single Condition IV fault shall not cause a consequential loss of required functions of systems needed to cope with the fault including those of the Reactor Coolant System and the Reactor Containment System.

Events

- Large LOCA
- Steam Generator Tube Rupture
- Main Steam Line Break (MSLB)
- Locked Rotor
- RCCA Ejection
- Fuel Handling

Acceptance Criteria

- Dose Consequences < 10CFR100
- RCS Pressure < 2900 psia (emergency)
- < 4000 psia (faulted)
- Containment Pressure and Temperature < Design Limits

The following events have event specific limits that are more limiting than the Condition IV criteria:

- Main Steam Line Break
  MDNBR > MDNBR Limit (MSLB)
- Locked Rotor
  Peak Clad Temperature < 2700°F
  Percentage of Fuel Rods Experiencing DNB < 40%
- RCCA Ejection
  Peak Clad Temperature < 2700°F
  Average Fuel Enthalpy < 200 cal/g

The basic principle applied in relating design requirements to each of the conditions is that the most frequent occurrences must yield little or no radiological risk to the public and those extreme situations having the potential for the greatest risk to the public shall be those least likely to occur. Where applicable, Reactor Protection System and Engineered Safeguards functioning is assumed to the extent allowed by considerations such as the single failure criterion in fulfilling this principle.

In the evaluation of the radiological consequences associated with initiation of a spectrum of accident conditions numerous assumptions must be postulated. In many instances, these assumptions are a product of extremely conservative judgments. This is due to the fact that many physical phenomena, in particular fission product transport under accident conditions, are not understood to the extent that accurate predictions can be made. Therefore, the set of assumptions postulated would predominantly determine the accident classification.

This section is divided into three subsections, dealing with various behavior categories:

- Core and Coolant Boundary Protection Analysis, Section 14.1
  The abnormalities presented in Section 14.1 have no off-site radiation consequences.

- Standby Safety Features Analysis, Section 14.2
  The accidents presented in Section 14.2 are more severe than those discussed in 14.1 and may cause release of radioactive material to the environment.

- Rupture of a Reactor Coolant Pipe, Section 14.3
  The accident presented in Section 14.3, the rupture of a reactor coolant pipe, is the worst-case accident analyzed and is the primary basis for the design of engineered safety features. It is shown that the consequences of even this accident are within the guidelines of 10 CFR100.

SAFETY ANALYSIS ASSUMPTIONS

Parameters and assumptions that are common to the safety analyses are described below to avoid repetition in subsequent sections.

Operating Parameters

For accident evaluation, the initial conditions are obtained by adding maximum steady-state errors to rated values. The following initial conditions and steady-state errors are considered:

Power	1650 MWt	± 2% of 1650 MWt for calorimetric error
Core Inlet Temperature	535.5°F	± 4°F for deadband and measurement error
Primary Pressure	2250 psia	+ 30 / −50 psi for steady-state fluctuations and measurement error

Initial values for power, primary pressure, and core temperature are selected to minimize the initial departure from nucleate boiling ratio (DNBR).

The initial active core flow rate is conservatively set to account for increased core bypass flow due to thimble plug removal and increased steam generator tube plugging. Unless otherwise stated in the Method of Analysis section for a particular accident the RCS and Core flow rates are set as follows:

Reactor Coolant System Flow	83,500 gal/min each loop
Core Inlet Flow	63.88E6 lb-m/hr
Core Bypass Flow	7.0%
Effective Core Flow	59.41E6 lb-m/hr

Hot Channel Factors

Unless otherwise stated in the sections describing specific accidents, the hot channel factors used are:

F_q^n (Nuclear Heat Flux Hot Channel Factor) = 2.35

$F_{\Delta H}^N$ (Nuclear Enthalpy Rise Hot Channel Factor) = 1.70

The movable in-core instrumentation system is employed to verify that actual hot channel factors are, in fact, no higher than the limiting values of the Technical Specifications. These limits on hot channel factors are designed to conservatively bound the assumptions used in the accident analyses.

Reactor Protection System

A reactor trip signal acts to open the two series trip breakers feeding power to the control rod drive mechanisms. The loss of power to the mechanism coils causes the mechanism to release the control rods, which then fall by gravity into the core. There are various instrumentation delays associated with each tripping function including delays in signal actuation, in opening the trip breakers and in the release of the rods by the mechanisms. The total delay to trip is defined as the time delay from the time that trip conditions are reached to the time the rods are free and begin to fall. The time delay and setpoint assumed for each tripping function used in the analysis are as follows:

Reactor Trip Function	Setpoint	Time Delay (sec)
Power Range Negative Rate	N/M*	N/A
Power Range Low Setpoint	35%	0.5
Power Range High Setpoint	118%	0.5
Overpower Delta T	120%	6.0
Overtemperature Delta T	119%	6.0
RCS Low Flow	87% of loop flow	0.6
High Pressurizer Level	100% of level span	1.5
Low Pressurizer Pressure	1735 psig	1.0
High Pressurizer Pressure	2410 psig	1.0
Low-Low Steam Generator Level	0.0% of level span	1.5
RXCP Undervoltage	N/M*	N/A
RXCP Underfrequency	N/M*	N/A
Turbine Trip	N/M*	N/A

N/M* - not explicitly modeled in safety analysis

The difference between the limiting trip setpoint assumed for the analysis and the actual trip setpoint represents a conservative allowance for instrumentation channel and setpoint errors. Results of surveillance tests demonstrate that actual instrument errors are equal to or less than the assumed values.

The instrumentation drift and calorimetric errors used in establishing the maximum overpower setpoint are presented in Table 14.0.1.

Trip is defined for analytical purposes as the insertion of all full-length rod control cluster assemblies (RCCAs) except the most reactive RCCA, which is assumed to remain in the fully withdrawn position. This is to provide shutdown margin capability against the remote possibility of a stuck RCCA condition existing at a time when shutdown is required.

The negative reactivity insertion following a reactor trip is a function of the acceleration of the control rods and the variation in rod worth as a function of rod position. Control rod positions after trip have been determined experimentally as a function of time using an actual prototype assembly under simulated flow conditions. The resulting rod positions were combined with rod worths to define the negative reactivity insertion as a function of time, as shown in Figure 14.0.1.

In summary, reactor protection is designed to prevent cladding damage in all transients and abnormalities. The most probable modes of failure in each protection channel result in a signal calling for the protective trip. Coincidence of two-out-of-three (or two-out-of-four) signals is required where single channel malfunction could cause spurious trips while at power. A single component or channel failure in the protection system itself coincident with one stuck RCCA is always permissible as a contingent failure and does not cause violation of the protection criteria. The reactor protection systems are designed in accordance with Reference 2.

Steam Generator Safety Valves

Unless otherwise stated in the section describing a specific accident, the following Steam Generator safety valve settings with 15% blowdown and rated safety valve flow capacities were assumed:

Valve	Nominal Safety Valve Setting (psig)	Safety Analysis Pressure Setpoint (psig) (Pressure at S/G)
1	1074	1150
2	1090	1167
3	1105	1167
4	1120	1183
5	1127	1193

Calorimetric Error Instrumentation Accuracy

The calorimetric error is the error assumed in the determination of core thermal power as obtained from secondary plant measurements. The total ion chamber current (sum of the top and bottom sections) is calibrated (set equal) to this measured power on a periodic basis. The

secondary power is obtained from measurement of feedwater flow, feedwater inlet temperature to the steam generator and steam pressure. High accuracy plant instrumentation is provided for these measurements with accuracy tolerances more restrictive than that which would be required to only control the feedwater flow. Each feedwater flow venturi is laboratory calibrated and certified. The expected accuracies are tabulated below with their effect on the overall power measurement.

Variable	Accuracy	Equivalent Percent of Rated Power
Feedwater temperature	± 2°F (½%)	
Feedwater pressure (Small correction on enthalpy)	± 5%	0.3% = Total effect
Steam pressure (Small correction on enthalpy)	± 2%	
Feedwater flow	± 1.25%	1.25%
		1.55% = Total error

Note that the errors have been added directly; statistical combination of errors indicate better accuracy. Corrections for moisture carry-over in the steam (0.25% design basis) can be made which would yield a lower measured power level. This effect can be conservatively neglected.

The secondary calorimetric is verified to be conservative as compared to the feedwater bypass line at the beginning of each cycle. The KNPP feedwater bypass line (FBL) is a full flow normal feedwater bypass loop designed to accurately measure total feedwater flow at KNPP. The FBL contains a flow section, which includes a flow straightener and a laboratory calibrated flow nozzle. The flow section is accurate to 0.25%.

The total uncertainty of this feedwater measurement is a function of the uncertainty of the FBL calibration, and the venturi repeatability. The uncertainty of total feedwater flow, as it contributes to the uncertainty of overall reactor power, is significantly less than the required 1.25%.

SAFETY ANALYSIS AND CORE RELOAD METHODOLOGY

By letter dated March 27, 1987, WPS submitted for NRC review a topical report entitled "Reload Safety Evaluation Methods for Application to Kewaunee". Additional information was submitted to the NRC on February 12 and March 7, 1988. The report includes methods for analyzing plant accidents, transients, and setpoints excluding the loss-of-coolant accident (LOCA) and the fuel mishandling accident. The NRC Safety Evaluation Report provided in Reference 3 reviewed the description and performance of the DYNODE-P (Version 5.4), the RETRAN-02, the VIPRE-01 and the TOODEE-2 codes employed in the analyses. In addition, the analyses, procedures and the results of specific calculations and reload evaluations were examined. The NRC found that the topical report was acceptable for referencing in KNPP licensing submittals.

REFERENCES - SECTION 14.0

1. ANSI N18.2-1973, "Nuclear Safety Criteria for the Design of Stationary Pressurized Water Reactor Plants"

2. IEEE 279, "Standard for Nuclear Plant Protection Systems", August 1968

3. NRC Safety Evaluation Report, JG Giitter (NRC) to DC Hintz (NRC), Letter No. K-88-67 dated April 11, 1988

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14.1 CORE AND COOLANT BOUNDARY PROTECTION ANALYSIS

The following anticipated events are abnormal operational transients resulting from component failure or operator error. They are anticipated to occur sometime in the design life of the plant.

In these events the reactor control and protection system and engineered safeguards are relied upon to protect the core and reactor coolant system boundary from damage.

- Uncontrolled RCCA Withdrawal from a Sub-critical Condition (Section 14.1.1)
- Uncontrolled RCCA Withdrawal at Power (Section 14.1.2)
- RCCA Misalignment (Section 14.1.3)
- Chemical and Volume Control System Malfunction (Section 14.1.4)
- Startup of an Inactive Reactor Coolant Loop (Section 14.1.5)
- Excessive Heat Removal Due to Feedwater System Malfunctions (Section 14.1.6)
- Excessive Load Increase Incident (Section 14.1.7)
- Loss of Reactor Coolant Flow (Section 14.1.8)
- Loss of External Electrical Load (Section 14.1.9)
- Loss of Normal Feedwater (Section 14.1.10)
- Loss of all AC Power to the Plant Auxiliaries (Section 14.1.12)

14.1.1 UNCONTROLLED RCCA WITHDRAWAL FROM A SUBCRITICAL CONDITION

Accident Description

A RCCA withdrawal incident is defined as an uncontrolled addition of reactivity to the reactor core by withdrawal of RCCAs resulting in a power excursion. While the probability of this type of a transient is extremely low, such a transient could be caused by a malfunction of the reactor control or control rod drive systems. This could occur with the reactor either sub-critical or at power. The "at power" case is discussed in Section 14.1.2.

Reactivity is added at a prescribed and controlled rate in bringing the reactor from a shutdown condition to a low power level during startup by RCCA withdrawal. Although the initial startup procedure used the method of boron dilution, the normal startup is with RCCA withdrawal. RCCA motion can cause much faster changes in reactivity than can be made by changing boron concentration.

The control rod drive mechanisms are wired into pre-selected bank configurations, which are not altered. The RCCAs are therefore physically prevented from withdrawing in other than their respective banks. Power supplied to the rod banks is controlled such that no more than two banks can be withdrawn at any time. The rod drive mechanism is of the magnetic latch type and the coil actuation is sequenced to provide variable speed rod travel.

The nuclear power response to a continuous reactivity insertion is characterized by a very fast rise terminated by the reactivity feedback effect of the negative fuel temperature coefficient. This self-limitation of the initial power burst results from a fast negative fuel

temperature feedback (Doppler effect) and is of prime importance during a startup accident, since it limits the power to a tolerable level prior to external control action. After the initial power burst, the nuclear power is momentarily reduced. If the accident is not terminated by a reactor trip, the nuclear power increases again, but at a much slower rate.

Should a continuous RCCA withdrawal be initiated, the transient will be terminated by the following automatic protection or control system actions:

a. Source Range High Neutron Flux Reactor Trip - This trip is actuated when either of two independent source range channels indicates a flux level above a pre-selected, manually adjustable value. This trip function may be manually bypassed when either intermediate range flux channel indicates a flux level above a specified setpoint. It is automatically reinstated when both intermediate range channels indicate a flux level below a specified setpoint.

b. Intermediate Range High Neutron Flux Rod Stop - This rod stop is actuated when either of two independent intermediate range channels indicates a flux level above a pre-selected, manually adjustable value. This rod stop may be manually bypassed when two out of the four power range channels indicate a power level above approximately 10% power. It is automatically reinstated when three of the four power range channels are below this value.

c. Although the actuation logic, bypass and automatic reinstatement conditions are the same for the Intermediate Range High Neutron Flux Rod Stop and Intermediate Range High Neutron Flux Reactor Trip, the rod stop is generated at ≤ 35% full power unless manually bypassed above permissive 10 (10% full power). The reactor trip will be actuated at ≤ 40% full power unless it has been manually bypassed above permissive 10.

d. Power Range High Neutron Flux Reactor Trip (low setting) - Trip is actuated when two out of the four power range channels indicate a power level above approximately 25%. This trip function may be manually bypassed when two of the four power range channels indicate a power level above approximately 10% power and is automatically reinstated when three of the four channels indicate a power level below this value.

e. Power Range High Neutron Flux Rod Stop - This rod stop is actuated when one-out-of-four power range channels indicates a power level above a preset setpoint. This function is always active.

f. Power Range High Neutron Flux Reactor Trip (high setting) - Trip is actuated when two-out-of-four power range channels indicate a power level above a preset setpoint. This trip function is always active.

Termination of the startup accident by the above protection channels prevents core damage. In addition, the reactor trip from high pressurizer pressure serves as a backup to terminate the accident before an overpressure condition could occur.

Method of Analysis

Analysis of this transient is performed by digital computation incorporating the neutron kinetics (including six delayed neutron groups) and the core thermal and hydraulic equations. In addition to the nuclear flux response, the average fuel, clad and water temperatures, and heat flux response were computed.

In order to give conservative results for a startup accident, the following assumptions are made concerning the initial reactor conditions:

a. Since the magnitude of the nuclear power peak reached during the initial part of the transient is for any given rate of reactivity insertion strongly dependent on the Doppler reactivity coefficient, a conservatively low negative value of −1.0E−5 Δk/°F is used for the startup accident. The less negative Doppler coefficient reduces the Doppler feedback effect, thereby increasing the nuclear flux peak.

b. The contribution of the moderator reactivity coefficient is negligible during the initial part of the transient because the heat transfer time constant between the fuel and the moderator is much longer than the nuclear flux response time constant. However, after the initial nuclear flux peak, the succeeding rate of power increase is affected by the moderator reactivity coefficient. A conservative value of +1.0E−4 Δk/°F has been used in the analysis since the positive value will yield the maximum peak core heat flux.

c. The reactor is assumed to be at hot zero power. This assumption is more conservative than that of a lower initial system temperature. The higher initial system temperature yields larger fuel to water heat transfer, larger fuel thermal capacity, and less negative (smaller absolute magnitude) Doppler coefficient. The high nuclear flux peak combined with a high fuel thermal capacity and large thermal conductivity yields a larger peak heat flux. The initial multiplication (k_o) is assumed to be 1.0 since this results in the maximum nuclear flux peak.

d. The most adverse combination of instrument and setpoint errors, as well as delays for trip signal actuation and rod release, are taken into account. A 10% increase has been assumed for the power range flux trip setpoint (low setting) raising it from the nominal value of 25% to 35%. Reference to Figure 14.1.1-1, however, shows that the rise in nuclear flux is so rapid that the effect of errors in the trip setpoint on the actual time at which the rods are released is negligible.

e. A maximum reactivity insertion rate is assumed (8.2E−4 Δk/sec) which is greater than that for the simultaneous withdrawal at maximum speed of the combination of the two RCCA banks having the greatest combined worth.

f. Initial power level of 1.0E−13 multiplied by the nominal full power level is assumed to maximize the heat flux peak.

Results

Figures 14.1.1-1 through 14.1.1-5 show the transient behavior of key parameters for a reactivity insertion rate of 8.2E–4 Δk/sec. The accident is terminated by a reactor trip at 35% power.

The nuclear power overshoots nominal full power, but only for a very short time period. Hence, the energy release and the fuel temperature increases are small. The heat flux response, of interest for DNB considerations, is shown in Figure 14.1.1-2. The beneficial effect of the inherent thermal lag of the fuel is evidenced by a peak heat flux that is less than the nominal full power heat flux. There is a large margin to DNB during the transient since the rod surface heat flux remains below the full power design value, and there is a high degree of sub-cooling at all times in the core. Figures 14.1.1-3, 14.1.1-4, and 14.1.1-5 show the response of the core average fuel, coolant and cladding temperature. The average fuel temperature increases to a value that is lower than the nominal full power value. The average coolant temperature increases to a value that is also less than the full power nominal value.

The following table shows the comparison of the important calculated safety parameters to their respective acceptance criteria (Calculated Value/Acceptance Criterion):

	MDNBR	RCS Pressure (psia)	MSS Pressure (psia)
Uncontrolled rod withdrawal from sub-critical	3.218/1.14	2358/2750	1156/1210

Conclusions

Considering the conservative assumptions used in the accident analysis, it is concluded that in the unlikely event of a control rod withdrawal accident the core and reactor coolant systems are not adversely affected. The peak heat flux reached remains less than the nominal full power value. DNBR is well above its limiting value. The peak average clad temperature is less than its nominal full power value, and thus there is no possibility of fuel or clad damage.

14.1.2 UNCONTROLLED RCCA WITHDRAWAL AT POWER

Accident Description

An uncontrolled RCCA withdrawal at power results in an increase in core heat flux. Since the heat extraction from the steam generator remains constant until the steam generator pressure reaches the relief or safety valve setpoint, there is a net increase in reactor coolant temperature. Unless terminated by manual or automatic action, the power mismatch and resultant coolant temperature rise would eventually result in DNB. Therefore, to prevent the possibility of damage to the cladding, the Reactor Protection System is designed to terminate any such transient before the DNBR falls below its limit.

The automatic features of the Reactor Protection System which prevent core damage in an RCCA withdrawal incident at power include the following:

1. Nuclear power range instrumentation actuates a reactor trip if two-out-of-four channels exceed an overpower setpoint.

2. Reactor trip is actuated if any two-out-of-four ΔT channels exceed an overtemperature ΔT setpoint. This setpoint is automatically varied with axial power distribution, temperature and pressure to protect against DNB.

3. Reactor trip is actuated if any two-out-of-four ΔT channels exceed an overpower ΔT setpoint. This setpoint is automatically varied with axial power distribution and temperature to ensure that the allowable fuel power rating is not exceeded.

4. A high-pressure reactor trip, actuated from any two-out-of-three pressure channels, is set at a fixed point. This set pressure is less than the set pressure for the pressurizer safety valves.

5. A high pressurizer water level reactor trip, actuated from any two-out-of-three level channels, is set at a fixed point. This affords additional protection for RCCA withdrawal incidents.

6. In addition to the above listed reactor trips, there are the following control rod assembly withdrawal blocks:

   - High nuclear power (one-out-of-four)
   - High overpower ΔT (two-out-of-four)
   - High overtemperature ΔT (two-out-of-four)

Method of Analysis

The purpose of this analysis is to demonstrate the manner in which the above protection systems function for various reactivity insertion rates from different initial conditions. Reactivity insertion rates and initial conditions govern which protective function occurs first.

Analysis is performed using several digital computer codes. The reactor protection functions are incorporated into the transient analysis digital simulation of the Nuclear Steam Supply System. The system response to the transient is then used as a transient forcing function for the fuel thermal hydraulic analysis and DNBR assessment.

In order to obtain conservatively low DNBRs, the following assumptions are made:

1. Initial conditions assume maximum power and reactor coolant temperatures and minimum pressure; i.e., the power is assumed 2% high, the average temperature is assumed 4°F high, and the pressure is assumed 50 psi low. This gives the minimum initial margin to DNB.

2. A zero moderator coefficient of reactivity is assumed corresponding to the beginning of core life. A conservatively small (in absolute magnitude) Doppler reactivity coefficient is used. The assumed reactivity coefficients result in a minimum of negative feedback reactivity, and therefore, higher peak powers and temperatures.

Results

Figures 14.1.2-1 through 14.1.2-4 show the response of nuclear power, pressure, average coolant temperature, and DNBR to a rapid RCCA withdrawal (8.2E–4 Δk/sec) incident starting from full power. This reactivity insertion rate is greater than that for the two highest worth banks, both assumed in their highest incremental worth region, withdrawn at their maximum speed. Reactor Trip on high nuclear power occurs less than 2.0 seconds from the start of the accident. Since this is rapid with respect to the thermal time constants small changes in T_{avg} and pressure result. A large margin to the MDNBR limit is maintained.

The response of nuclear power, pressure, average coolant temperature, and DNBR for a slow RCCA withdrawal (3.0E–5 Δk/sec) from full power is shown in Figures 14.1.2-5 through 14.1.2-8. Reactor Trip occurs on overtemperature ΔT. The rise in temperature and pressure is larger than for the rapid RCCA withdrawal. The minimum DNBR reached during the transient is greater than the MDNBR limit.

The nuclear power, RCS pressure, coolant average temperature, and DNBR responses for an RCCA withdrawal from 60% power are shown in Figures 14.1.2-9 through 14.1.2-12 for a rapid withdrawal rate (8.2E–4 Δk/sec) and in Figures 14.1.2-13 through 14.1.2-16 for a slow withdrawal rate (1.5E–5 Δk/sec). The results demonstrate that the overtemperature ΔT and high nuclear flux trip functions adequately protect the fuel. The minimum DNBR reached is above the MDNBR limit.

The following table shows the comparison of the important calculated safety parameters to their respective acceptance criteria (Calculated Value/Acceptance Criterion):

Uncontrolled rod withdrawal at power	MDNBR	RCS Pressure (psia)	MSS Pressure (psia)
Fast Rate Full Power	1.532/1.14	2257/2750	952/1210
Slow Rate Full Power	1.362/1.14	2313/2750	954/1210
Fast Rate Intermediate Power	1.999/1.14	2321/2750	962/1210
Slow Rate Intermediate Power	1.165/1.14	2350/2750	1182/1210

Conclusions

In the unlikely event of an RCCA withdrawal incident during power operation, the core and Reactor Coolant System are not adversely affected since the minimum value of the DNBR reached is greater than the DNBR limit for all RCCA reactivity rates. Protection is provided by the high nuclear flux, overpower ΔT, and overtemperature ΔT trip functions.

14.1.3 RCCA MISALIGNMENT

Accident Description

RCCA misalignment accidents include:

a. dropped full-length RCCAs;

b. dropped full-length RCCA banks; and

c. statically misaligned full-length RCCAs.

Each RCCA has a rod position indicator channel which displays position of the assembly. The displays of assembly positions are grouped for the operator's convenience. Fully inserted assemblies are further indicated by rod bottom lights. Bank demand position is also indicated. The full-length assemblies are always moved in pre-selected banks and the banks are always moved in the same pre-selected sequence.

Dropped assemblies or banks are detected by:

a. sudden drop in the core power level

b. asymmetric power distribution as seen on out-of-core neutron detectors or core exit thermocouples

c. rod bottom light(s)

d. rod deviation alarm (if the plant computer is in operation).

Misaligned assemblies are detected by:

a. asymmetric power distribution as seen on out-of-core neutron detectors or core exit thermocouples

b. rod deviation alarm (if the plant computer is in operation).

The resolution of the rod position indicator channel is ± 5% of span or 7.2 inches (span equals 12 feet). Deviation of any assembly from its bank by twice this distance, 10% of span, or 14.4 inches, will not cause power distributions worse than the design limits.

If one or more rod position indicator channels is not operable, the operator will be fully aware of the inoperability of the channel, and special surveillance of core power tilt indications, using established procedures and relying on ex-core nuclear detectors and/or movable in-core detectors, will be used to verify power distribution symmetry.

Method of Analysis

The safety analysis for the RCCA misalignment and RCCA drop accidents involves a full power fuel thermal hydraulic analysis. The peak fuel rod $F_{\Delta H}$ in the analysis is increased to adequately bound the core power distribution anticipated during the steady state RCCA misalignment and RCCA drop core conditions. The reactor is assumed not to trip.

It is necessary to show on a reload cycle specific basis that the worst dropped or misaligned RCCA does not result in a peak fuel rod power ($F_{\Delta H}$) that is greater than the $F_{\Delta H}$ assumed in the safety analysis.

Rod drop in automatic control is not analyzed since restrictions on control rods in automatic control are imposed when reactor power is > 90% and control rods are inserted to < 215 steps. This restriction ensures that the rod drop in automatic control accident is bounded by the static RCCA misalignment described above. Removal of the rod drop in automatic control was determined to be acceptable by the NRC in Reference 1.

Results

The following table shows the comparison of the important calculated safety parameters to their respective acceptance criteria (Calculated Value/Acceptance Criterion):

	MDNBR	RCS Pressure (psia)	MSS Pressure (psia)
Control Rod Drop and Misalignment $F_{\Delta H} = 2.02$	1.142/1.14	2250/2750	750/1210

Conclusions

Dropped or misaligned RCCAs are not deemed to be a hazard to the safe operation of the plant because these events are clearly indicated to the operator, and the analyzed cases of the worst misaligned and dropped rod do not result in a DNBR less than the MDNBR limit.

For all cases of dropped banks, the reactor is tripped by the power range negative neutron flux rate trip and consequently dropped banks do not cause core damage.

14.1.4 CHEMICAL AND VOLUME CONTROL SYSTEM MALFUNCTION

Accident Description

Reactivity can be added to the core with the Chemical and Volume Control System by feeding reactor makeup water into the Reactor Coolant System via the Reactor Makeup Control System. Boron dilution is a manual operation. A boric acid blend system is provided to permit the operator to match the concentration of reactor coolant makeup water to that existing in the coolant at the time.

The Chemical and Volume Control System is designed to limit, even under various postulated failure modes, the potential rate of dilution to a value that after indication through alarms and instrumentation, provides the operator sufficient time to correct the situation in a safe and orderly manner.

The source of reactor makeup water for the Reactor Coolant System is the reactor makeup water storage tanks. Inadvertent dilution can be readily terminated by isolating this source. The operation of the reactor makeup water pumps, which take suction from these tanks, provides the only supply of makeup water to the Reactor Coolant System. In order for makeup to be added to the Reactor Coolant System the charging pumps must be running in addition to the reactor makeup water pumps.

There are three positive displacement variable speed drive charging pumps, manually or automatically controlled. When in automatic, each is provided with a high and low speed alarm. However, only one of them is automatically controlled at any one time, as dictated by procedure.

The rate of addition of unborated makeup water to the reactor coolant system is limited by the capacity of the charging pumps and by the capacity of the control valve between the two makeup water pumps and the three charging pumps. The maximum dilution flow (80 gpm) occurs with two charging pumps operating and three letdown orifices in-service. For the purpose of this analysis, a larger, unrealistic flow rate (180 gpm) is used, corresponding to all three charging pumps operating at full flow. During normal operation, two charging pumps are operated; one in manual and one in automatic control. The speed of the pump selected for automatic control is controlled by the pressurizer level error signal. During load changes the pressurizer level set point varies automatically with T_{avg} such that the charging pump speed remains relatively constant.

The boric acid from the boric acid tank is blended with the reactor makeup water in the blender and the composition is determined by the preset flow rates of boric acid and reactor makeup water on the Reactor Makeup Controller. Two separate operations are required. First, the operator must switch from the automatic makeup mode to the dilute mode. Second, the control switch must be actuated. Omitting either step would prevent dilution. This makes the probability of inadvertent dilution very small.

Information on the status of the reactor coolant makeup is continuously displayed. Lights are provided on the control board to indicate the operating condition of pumps in the Chemical and Volume Control System. Alarms are actuated to warn the operator if boric acid or demineralized water flow rates deviate from pre-set values as a result of system malfunction.

To cover all phases of plant operation, boron dilution during refueling, startup, and power operation are considered in this analysis.

Method of Analysis and Results

Dilution During Refueling

During refueling the following conditions exist:

a. One residual heat removal pump is running to ensure continuous mixing in the reactor vessel,

b. The valve in the seal water header to the reactor coolant pumps is closed,

c. The valves on the suction side of the charging pumps are adjusted for addition of concentrated boric acid solution,

d. The boron concentration of the refueling water is a minimum of 2200 ppm, corresponding to a shutdown of at least 5% $\Delta k/k$ with all control rods in; periodic sampling ensures that this concentration is maintained,

e. The source range detectors outside the reactor vessel are active and provide an audible count rate.

The operator has prompt and definite indication of any boron dilution from the audible count rate instrumentation. High-count rate is alarmed in the reactor containment and the main control room. The count rate increase is proportional to the inverse core multiplication factor. Assuming the reactor is 5% shutdown at the required refueling boron concentration of 2200 ppm, the time to reach critical conditions is > 30 minutes. This is ample time for the operator to recognize the audible high-count rate signal and isolate the reactor makeup water source by closing valves and stopping the reactor makeup water pumps.

Dilution During Startup

During startup the following are assumed for a boron dilution event:

- Core monitoring of neutron flux is provided by the excore detectors.
- Reactor coolant is mixed by operation of the reactor coolant pumps.
- Three charging pumps are running, delivering a maximum dilution flow rate of 180 gpm.
- The boron endpoint with all rods inserted is 1300 ppm.
- Initial reactor boron concentration is 2200 ppm.

An evaluation of the reactor shows that the minimum time required to reduce the reactor coolant boron concentration to a concentration at which the reactor could go critical with all RCCAs in is > 15 minutes. This provides adequate time for the operator to respond to the high-count rate signal and terminate dilution flow.

Dilution at Power

The reactivity addition rate corresponds to a boron dilution flow of 180 gpm at full power conditions with all three charging pumps running. This is a conservatively high boron dilution flow and reactivity insertion rate for the assumed at power boron concentration of 1600 ppm.

With the reactor in automatic control, at full power, the power and temperature increase from the boron dilution results in the insertion of the controlling RCCA bank and a decrease in shutdown margin. A continuation of the dilution and RCCA insertion would cause the rods to reach the lower limit of the maneuvering band. Before reaching this point, however, two alarms would be actuated to warn the operator of the potential accident condition. These two alarms, the low RCCA insertion limit alarm and the low-low RCCA insertion limit alarm, alert the operator to initiate normal boration.

With no boration, the required shutdown margin is maintained for at least 10 minutes during a continuous boron dilution. Therefore, ample time is available following the alarms for the operator to determine the cause, isolate the reactor water makeup source, and initiate reboration.

If rod control is in manual, and the operator takes no action, the power rises to the high neutron flux trip setpoint and the reactor trips. Figures 14.1.4-1 through 14.1.4-5 show the response of nuclear power, pressure, coolant average temperature, heat flux, and DNBR to a boron dilution event in manual control. The boron dilution in this case is essentially identical to a rod withdrawal accident. The reactivity insertion rate due to the boron dilution is within the range of reactivity insertion rates considered in Section 14.1.2 - Uncontrolled RCCA Withdrawal at Power. Assuming a 1% shutdown margin, there is ample time available for the operator to terminate the dilution before the reactor can return to criticality following the trip.

The following table shows the comparison of the important calculated safety parameters to their respective acceptance criteria (Calculated Value/Acceptance Criterion):

	MDNBR	RCS Pressure (psia)	MSS Pressure (psia)
Chemical & Volume Control System Malfunction	1.347/1.14	2324/2750	956/1210

Conclusions

Because of the procedures involved in the dilution process, an erroneous dilution is considered unlikely. Nevertheless, if an unintentional dilution of boron in the reactor coolant does occur, numerous alarms and indications are available to alert the operator to the condition. The maximum reactivity addition rate due to the dilution is slow enough to allow the operator adequate time to determine the cause of the dilution and take corrective action before required shutdown margin is lost. The dilution event at power is shown to have adequate margin to the MDNBR limit.

14.1.5 STARTUP OF AN INACTIVE REACTOR COOLANT LOOP

Accident Description

Operation of the plant with an inactive loop causes reversed flow through the inactive loop because there are no isolation valves or check valves in the reactor coolant loops.

If the reactor is operated at power in this condition there is reverse flow through the inactive loop due to the pressure difference across the reactor vessel. The cold leg temperature in the inactive loop is identical to the cold leg temperatures of the active loop and to the reactor core inlet temperature. If the reactor is operated at power, there is a temperature drop across the steam generator in the inactive loop, and with the reverse flow, the hot leg temperature of the inactive loop is lower than the reactor core inlet temperature.

The protection system prohibits continuous operation of the plant above approximately 10% with one inactive loop. The starting of the idle reactor coolant pump results in the injection of cold water into the core and this causes a rapid reactivity and power increase. However, for power on the order of 10%, the hot leg temperature of the inactive loop is close to the core inlet temperature, thus limiting the severity of the resulting transient.

Assumptions and Method of Analysis

The following assumptions are made:

a. Following the start of the idle pump, the inactive loop flow accelerates linearly to its nominal full flow value over a period of 10 seconds.

b. A conservative negative moderator coefficient of $-4.0E-4$ $\Delta k/°F$ is assumed.

c. A conservative low Doppler temperature coefficient of $-1.0E-5$ $\Delta k/°F$ is assumed.

d. The reactor is assumed to be initially at 12% of 1650 MWt with the secondary side of both steam generators at the same pressure and with reverse reactor coolant flow through the idle loop steam generator. The 12% includes 2% allowance for calibration and instrument errors. The high initial power assumed is conservative since it gives the greatest temperature difference between the core inlet temperature and the inactive loop hot leg temperature.

e. The initial Reactor Coolant System average temperature in the active loops is 4°F above the programmed value for 12% power. This is a conservatively high value for the initial average temperature including instrument errors and results in the minimum margin to core DNB limits.

f. The initial Reactor Coolant System pressure is 50 psi below nominal. This is a conservatively low value for the initial pressure including instrument errors and results in the minimum margin to core DNB limits.

A detailed digital simulation of the plant, including heat transfer to the steam generators of the active and inactive loop, and reactor coolant flow transit times, was used to study the transient following pump startup in the inactive loop.

Results

The results following the startup of an idle loop with the assumptions listed above are shown in Figures 14.1.5-1 through 14.1.5-5. The heat flux response, of interest for DNB considerations, indicates that the peak heat flux reaches a value that is less than the nominal full power value. This low heat flux combined with a high degree of sub-cooling in the core at all times results in no adverse effects to the core by the transient. No reactor trip occurs.

It is expected that the actual transient effects would be less severe than those shown because of alleviating factors, which have not been taken into account. For example, the actual starting time of the Reactor Coolant Pump is likely to be about 20 seconds rather than the 10 seconds assumed in the analysis. This means that the change in core temperature would occur more gradually than shown in the figures. Furthermore, the water entering the core is assumed to exhibit the temperature of the water in the inactive loop, providing the analysis with a high degree of conservatism.

The average temperature of the reactor coolant water increases because of the positive reactivity insertion and power increase brought about by the entry into the core of the cold water in the inactive loop. This leads to an increase in pressurizer pressure. The maximum pressure reached is well below the acceptance criteria of 2750 psia.

The following table shows the comparison of the important calculated safety parameters to their respective acceptance criteria (Calculated Valve/Acceptance Criterion):

	MDNBR	RCS Pressure (psia)	MSS Pressure (psia)
Startup of Inactive Loop	5.878/1.14	2313/2750	1153/1210

Conclusions

The results show that for startup of an inactive loop, the power and the temperature excursions are not severe. There is a considerable margin to the limiting MDNBR. Therefore, no undue restriction needs to be placed on the plant when starting a reactor coolant pump at power levels up to 12% power.

14.1.6 EXCESSIVE HEAT REMOVAL DUE TO FEEDWATER SYSTEM MALFUNCTIONS

Accident Description

Reductions in feedwater temperature or additions of excessive feedwater are means of increasing core power above full power. Such transients are attenuated by the thermal capacity

of the secondary system and of the Reactor Coolant System. The Reactor Protection System trip functions prevent any power increase that could lead to a DNBR less than the MDNBR limit.

An extreme example of excessive heat removal from the Reactor Coolant System is the transient associated with the accidental opening of the feedwater bypass valve, which diverts flow around the low-pressure feedwater heaters. The function of this valve is to maintain net positive suction head on the main feedwater pump in the event that the heater drain pump flow is lost, e.g., following a large load decrease.

In the event of an accidental opening, there is a sudden reduction in feedwater inlet temperature to the steam generators. This increased sub-cooling would create a greater load demand on the Reactor Coolant System due to the increased heat transfer in the steam generator.

Another example of excessive heat removal from the Reactor Coolant System is a common mode failure in the feedwater control system, which leads to the accidental opening of the feedwater regulating valves (FW-7A and FW-7B) to both steam generators (see Reference 2).

FW-7A and FW-7B could fail open due to a high output signal to the feedwater control system from any one of the following components:

- PT-485 First Stage Turbine Pressure Transmitter
- PM-485A I/I Converter
- LM-463F Steam Generator Level Auto Programmer Mode Controller
- LM-463H Steam Generator Level Program Median Selector
- LM-463D Current Source for Steam Generator Level Minimum Setpoint
- LM-463C Lead/Lag Circuit

This results in the valves stepping open 20% from their current position followed by a 20% step open every 5 minutes after that until full open.

Accidental opening of the feedwater regulating valves results in an increase of feedwater flow to both steam generators, causing excessive heat removal from the reactor coolant system. The resultant decrease in the average temperature of the core causes an increase in core power due to moderator and control system feedback.

Continuous addition of cold feedwater after a reactor trip is prevented since the reduction of Reactor Coolant System temperature, pressure, and pressurizer level leads to the actuation of safety injection on low pressurizer pressure. The safety injection signal trips the main feedwater pumps, closes the feedwater pump discharge valves, and closes the main feedwater control valves.

Accidental Opening of the Feedwater Bypass Valve

Method of Analysis

Two cases have been analyzed to demonstrate the plant behavior in the event of a sudden feedwater temperature reduction resulting from the accidental opening of the feedwater bypass valve. The first case is for a reactor in manual control with a zero moderator reactivity coefficient since this represents a condition in which the plant has the least inherent transient capability. The second case is for a reactor in automatic control with a conservatively large negative moderator reactivity coefficient (–4.0E–4 Δk/°F).

Initial pressurizer pressure, reactor coolant average temperature, and reactor power are consistent with steady state, full power operation, allowing for calibration and instrument errors. This results in the minimum margin to core DNB at the start of the transient. The analyses are performed using a detailed digital simulation of the plant including core kinetics, Reactor Coolant System, and the Main Steam and Feedwater Systems.

Results

Figures 14.1.6-1 through 14.1.6-5 show the transient without automatic reactor control and with a zero moderator reactivity coefficient representing beginning of cycle conditions. As expected, the average reactor coolant temperature and pressurizer pressure show rapid decreases as the secondary heat extraction remains greater than the core power generation. The core power level increases slowly and eventually comes to equilibrium at a value slightly above the nominal full power value. There is an increased margin to DNB because of the accompanying reduction in coolant average temperature. The reactor does not trip. There is a small increase in core ΔT as the heat transfer increases through the steam generator.

Figures 14.1.6-6 through 14.1.6-10 illustrate the transient with automatic reactor control. A conservatively large negative moderator coefficient (–4.0E–4 Δk/°F) representing end of cycle core conditions is assumed. The large negative moderator coefficient increases reactor power, which reduces the decrease in temperature and pressure. Eventually reactor power comes to equilibrium at a value slightly above the nominal full power value. The minimum DNB ratio decreases slightly but is well above the MDNBR limit.

The reactivity insertion rate at no-load from an excessive feedwater flow increase accident is also analyzed with the following assumptions:

1. A step increase in feedwater flow to one steam generator from zero to the nominal full-load flow.

2. The most negative reactivity moderator coefficient at end-of-life.

3. A constant feedwater temperature of 70°F

4. Heat capacity of the Reactor Coolant System and steam generator shell are not taken credit for

5. Neglect of the energy stored in the fluid of the unaffected steam generator.

The maximum reactivity insertion rate was calculated to be 2.3E–4 Δk/sec which is less than the maximum reactivity insertion rate analyzed in Section 14.1.1, Uncontrolled RCCA Withdrawal from a Sub-critical Condition. It should be noted that if the incident occurs with the reactor critical at no-load, the reactor would be tripped by the power range high nuclear flux trip (low setting) set at approximately 25%. As shown in Section 14.1.1, there is a large margin to DNB with the above calculated reactivity insertion rate.

The following table shows the comparison of the important calculated safety parameters to their respective acceptance criteria (Calculated Value/Acceptance Criterion):

Feedwater System Malfunction	MDNBR	RCS Pressure (psia)	MSS Pressure (psia)
BOC Manual Control	1.681/1.14	2200/2750	751/1210
EOC Auto Control	1.647/1.14	2200/2750	751/1210

Accidental Opening of Feedwater Regulating Valves

Method of Analysis

The following assumptions are made for the analysis of for a feedwater malfunction event involving the accidental opening of the feedwater regulating valves:

1) The plant is operating at full power allowing for instrument and calibration uncertainties level with the feedwater control system in automatic mode. The safety analysis uses this power level to give the highest feedwater flow rate.

2) Automatic steam generator level control is functional with the exception of the failed valves.

3) The feedwater in headers A and B is at a temperature of 430°F. This temperature is consistent with normal plant conditions.

4) Feedwater flow increases 50% in both loops from 3.6 to 5.4 MLBM/HR. This is conservative because pump runout flow is 5.0 MLBM/HR.

The analysis is performed using a detailed digital simulation of the plant including core kinetics, Reactor Coolant System and the Main Steam and Feedwater Systems.

Results

Reactor power increases to slightly above the nominal full power value due to the reactor cooldown, which is caused by the excessive feedwater flow to both steam generators. As a result, minimum DNBR decreases slightly but is well above the limiting minimum DNBR.

Conclusions

Feedwater system malfunction transients involving a reduction in feedwater temperature or an increase in feedwater flow rate have been analyzed. The analyses show an increase in reactor power from the reactor temperature reduction due to the excessive heat removal in the steam generators. The most limiting of the feedwater malfunction transients is the inadvertent opening of a feedwater heater bypass valve at full power conditions. Analyses demonstrate that considerable margin to the safety analysis acceptance criteria, (MDNBR, primary and secondary pressure), exists throughout the transient. Therefore, there is no radioactive release or public hazard in the event of a feedwater malfunction event.

14.1.7 EXCESSIVE LOAD INCREASE INCIDENT

Accident Description

An excessive load increase incident is defined as a rapid increase in steam generator steam flow that causes a power mismatch between the reactor core power and the steam generator load demand. The Reactor Control System is designed to accommodate a 10% step load increase or a 5% per minute ramp load increase (without a reactor trip) in the range of 15 to 95% of full power. Any loading rate in excess of these values may cause a reactor trip actuated by the Reactor Protection System. If the load increase exceeds the capability of the Reactor Control System; the transient is terminated in sufficient time to prevent the DNBR from being reduced below the MDNBR limit. An excessive load increase incident could result from either an administrative violation such as excessive loading by the operator or an equipment malfunction in the steam dump control or turbine speed control.

For excessive loading by the operator or by system demand, the turbine load limiter keeps maximum turbine load from exceeding 100% rated load.

During power operation, steam dump to the condenser is controlled by reactor coolant condition signals; i.e., high reactor coolant temperature indicates a need for steam dump. A single controller malfunction does not cause steam dump; an interlock is provided which blocks the opening of the valves unless a large turbine load decrease or a turbine trip has occurred.

Load increases caused by a hypothetical steam-line break are analyzed in Section 14.2.5.

Method of Analysis

Four cases are analyzed to demonstrate the plant behavior for a 20% step increase from rated load. The first two cases are for a manually controlled reactor at beginning of cycle (BOC, α_m = zero $\Delta k/°F$) and end of cycle (EOC, $\alpha_m = -4.0E-4\ \Delta k/°F$) conditions ($\alpha_m$ is the moderator reactivity co-efficient). Beginning of cycle represents a condition when the plant has the smallest moderator temperature coefficient of reactivity and, therefore, the least inherent transient capability. Two cases are analyzed for an automatic control situation at BOC and EOC conditions with control rods initially inserted to the power dependent insertion limits. A conservative limit on the turbine valve opening was assumed corresponding to 1.2 times nominal steam flow at nominal steam pressure. Initial pressurizer pressure, reactor coolant average temperature and power are assumed at extreme values consistent with steady state, full-power operation, allowing for calibration and instrument errors. This results in the minimum margin to core DNB at the start of the transient. The analyses are performed using a detailed digital simulation of the plant including core kinetics, Reactor Coolant System, and the Steam and Feedwater Systems.

Results

Figures 14.1.7-1 through 14.1.7-8 illustrate the transient with the reactor in the manual control mode. As expected, for the BOC case with a very slight power increase, the core average temperature shows a large decrease. For the EOC case, there is a much larger increase in reactor power due to the moderator feedback. Both of the manual control cases demonstrate adequate MDNBR margin.

Figures 14.1.7-9 through 14.1.7-18 illustrate the transient assuming the reactor is in automatic control. In automatic control the reactor power transient is greater than for the corresponding case in manual control. The automatic control cases still show adequate margin to the MDNBR limit.

The following table shows the comparison of the important calculated safety parameters to their respective acceptance criteria (Calculated Value/Acceptance Criterion):

Excessive Load Increase	MDNBR	RCS Pressure (psia)	MSS Pressure (psia)
BOC Manual Control	1.681/1.14	2200/2750	751/1210
BOC Auto Control	1.430/1.14	2200/2750	751/1210
EOC Manual Control	1.478/1.14	2200/2750	751/1210
EOC Auto Control	1.438/1.14	2200/2750	751/1210

Conclusions

The four cases analyzed show a considerable margin to the limiting MDNBR. It is concluded that reactor integrity is maintained throughout lifetime for the excessive load increase incident.

14.1.8 LOSS OF REACTOR COOLANT FLOW

Accident Description

A loss-of-coolant flow incident can result from a mechanical or electrical failure in one or more reactor coolant pumps (RXCPs), or from a fault in the power supply to these pumps. If the reactor is at power at the time of the incident, the immediate effect of loss-of-coolant flow is a rapid increase in coolant temperature. This increase could result in DNB with subsequent fuel damage if the reactor is not tripped promptly. The following trip circuits provide the necessary protection against a loss-of-coolant flow incident:

- Low voltage on pump power supply bus
- Pump circuit breaker opening (low frequency on pump power supply bus opens pump circuit breaker)
- Low reactor coolant flow

These trip circuits and their redundancy are further described in Section 7.2, Reactor Control and Protection System.

Simultaneous loss of electrical power to all RXCPs at full power is the most severe credible loss-of-coolant flow condition. For this condition, reactor trip together with flow sustained by the inertia of the coolant and rotating pump parts will be sufficient to prevent fuel failure, Reactor Coolant System overpressure, and prevent the DNBR from going below its limit.

Two types of flow coastdown accidents were analyzed, loss of two RXCPs at nominal frequency and loss of two RXCPs at low frequency. These two types of flow coastdown analyses are described separately under Loss of Reactor Coolant Flow-Nominal Frequency and under Loss of Reactor Coolant Flow Low Frequency.

Loss of Reactor Coolant Flow-Nominal Frequency

Method of Analysis

The following nominal frequency loss of coolant flow case is analyzed: Loss of two pumps from a Reactor Coolant System heat output of 102% of 1650 MWt with two loops operating. This case represents the worst credible coolant flow loss.

The normal power supplies for the pumps are the two buses connected to the generator, each of which supplies power to one of the two pumps. When a generator trip occurs, the pumps are automatically transferred to a bus supplied from external power lines. Therefore, the simultaneous loss of power to both reactor coolant pumps is a highly unlikely event.

Following any turbine trip, when there are no electrical faults requiring tripping the generator from the grid, the generator remains connected to the grid for at least thirty seconds. Since both pumps are not on the same bus, a single bus fault does not result in the loss of both pumps.

A full plant simulation is used in the analysis to compute the core average and hot spot heat flux transient responses, including flow coastdown, temperature, reactivity, and control rod insertion effects.

These data are then used in a detailed thermal hydraulic computation to compute the margin to DNB. This computation solves the continuity, momentum, and energy equations of fluid flow together with the DNB correlation. The following assumptions are made in the calculations:

a. The initial operating conditions, which are assumed to be most adverse with respect to the margin to DNB, are maximum steady-state power level, minimum steady-state pressure, and maximum steady-state inlet temperature.

b. The largest negative initial value of the Doppler coefficient (–2.32E–5 Δk/°F) and a zero moderator coefficient (0.0 Δk/°F) are assumed since these result in the maximum heat flux during the initial part of the transient, when the minimum DNB ratio is reached.

c. A reactor trip is actuated by low flow. The time from the initiation of low-flow signal to initiation of RCCA motion is 0.6 seconds. The trip signal is assumed to be initiated at 87% of full-loop flow, allowing at least 3% for flow instrumentation errors.

   Upon reactor trip, it is also assumed that the most reactive RCCA is stuck in its fully withdrawn position, hence resulting in a minimum insertion of negative reactivity.

d. The overall heat transfer between the fuel and the water varies considerably during the transient mostly as a result of the change of fuel gap conductance. A conservatively evaluated overall heat transfer coefficient is used in the analysis.

Results

Reactor coolant flow coastdown curve is shown in Figure 14.1.8-1. Reactor coolant flow is calculated based on a momentum balance in the Reactor Coolant System combined with a pump momentum balance.

The following table shows the comparison of the important calculated safety parameters to their respective acceptance criteria (Calculated Value/Acceptance Criterion):

Loss of Flow	MDNBR	RCS Pressure (psia)	MSS Pressure (psia)
2/2 Pump Trip	1.291/1.14	2303/2750	908/1210

Figures 14.1.8-2 and 14.1.8-3 show the nuclear power and the average heat flux response for the two-pump loss of flow. Figure 14.1.8-4 shows the MDNBR as a function of time.

Loss of Coolant Flow - Low Frequency

Method of Analysis

The underfrequency event is analyzed using a systems analysis that calculates the loop and core flow, nuclear power, and primary system pressure and temperature transients. The MDNBR is calculated by performing a detailed fuel thermal hydraulic simulation using as transient forcing functions the core heat flux, core flow, core inlet temperature, and Reactor Coolant System pressure from the systems analysis.

a. The initial operating conditions, which are assumed to be most adverse with respect to the margin to DNB, are maximum steady-state power level, minimum steady-state pressure, and maximum steady-state average temperature.

b. A conservatively large absolute value of the Doppler only power co-efficient and a zero moderator coefficient (0.0 $\Delta k/°F$) are assumed since these result in the maximum hot channel heat flux during the initial part of the transient, when the MDNBR is reached.

c. A constant frequency decay rate of 5 Hz/sec is assumed. Reference 3 determined that this is the maximum credible frequency decay rate that could occur on a typical electrical grid. Analysis of the Wisconsin-Upper Michigan transmission system indicates that the worst-case frequency decay rate is be approximately 2 Hz/sec (see Reference 4). Therefore, 5 Hz/sec is a very conservative decay rate. In addition, the assumption of a constant rate is conservative, since Reference 3 also shows that the expected grid frequency decay rate actually decreases during the transient.

   Prior to the opening of the RXCP breaker, the RXCP speed is assumed to be directly proportional to the power supply frequency. As discussed in Reference 5, this is a conservative assumption, since the speed coastdown will lag the frequency coastdown due to the effects of pump inertia and induction motor slip. During steady state operation the pump motor speed is below the synchronous speed because of induction motor slip. After the frequency decay starts, the deceleration of the pump-motor-flywheel combination provides a positive driving torque to the pump so that the required electrical torque decreases. The reduction in electrical torque reduces the induction motor slip, thus resulting in a higher speed than that assumed in the analysis. The degree of conservatism varies directly with the assumed decay rate because the inertia torque increases directly with the decay rate. At 5 Hz/sec the expected speed is approximately 1.2% higher than the analysis value.

   Reactor Coolant System flow is calculated based on a momentum balance in the Reactor Coolant System combined with a pump momentum balance.

d. No credit is taken for the RXCP trip on underfrequency.

e. Upon reactor trip, it is assumed that the most reactive RCCA is stuck at its fully withdrawn position, resulting in a minimum insertion of negative reactivity.

In Reference 7, the NRC approved use of the WPS loss of flow underfrequency trip methodology.

Results

The following table shows the comparison of the important calculated safety parameters to their respective acceptance criteria (Calculated Value/Acceptance Criterion):

Loss of Flow	MDNBR	RCS Pressure (psia)	MSS Pressure (psia)
Underfrequency Trip	1.207/1.14	2378/2750	879/1210

Figures 14.1.8-5 through 14.1.8-8 shows the nuclear power, average channel heat flux, core flow, and MDNBR transient responses for the underfrequency event.

MDNBR is always above the MDNBR limit. Therefore, fuel rod integrity and safe plant shutdown are ensured by an underfrequency trip setting of 54.5 Hz.

Conclusions

Since DNB does not occur, there is no cladding damage and no release of fission products into the reactor coolant. Therefore, once the fault is corrected the plant can be returned to service in the normal manner. The absence of fuel failures would, of course, be verified by analysis of reactor coolant samples. In the loss of reactor coolant flow accidents, it has been shown that there is adequate reactor coolant flow to maintain a MDNBR greater than the MDNBR limit.

Locked Rotor Accident

Accident Description

A transient analysis is performed for the hypothetical instantaneous seizure of a reactor coolant pump rotor. Flow through the Reactor Coolant System is rapidly reduced, leading to a reactor trip on a low-flow signal. Following the trip, heat stored in the fuel rods continues to pass into the core coolant causing the coolant to expand. At the same time, heat transfer to the shell side of the steam generator is reduced, first because the reduced flow results in a decreased tube side film coefficient and then because the reactor coolant in the tubes cools down while the shell side temperature increases (turbine steam flow is reduced to zero upon plant trip). The rapid expansion of the coolant in the reactor core, combined with the reduced heat transfer in the steam generator causes an insurge into the pressurizer and a pressure increase throughout the Reactor Coolant System. The insurge into the pressurizer compresses the steam volume, actuates the automatic spray system, opens the power-operated relief valves, and opens the pressurizer safety valves, in that sequence. The two power-operated relief valves are designed for reliable operation and would be expected to function properly during the accident. However, for conservatism, their pressure-reducing effect is not included in the analysis.

Method of Analysis

At the beginning of the postulated locked rotor accident, i.e., at the time the shaft in one of the RXCPs seizes, the plant is assumed to be in operation under the steady-state operating conditions that are most adverse with respect to MDNBR margin. The plant is assumed to be operating at maximum steady-state power, minimum steady-state pressure, and maximum steady-state core inlet temperature.

After pump seizure, nuclear power is rapidly reduced because of void shutdown and the RCCA insertion upon reactor trip.

No credit is taken for the pressure reducing effect of the pressurizer relief valves, pressurizer spray, steam dump, or controlled feedwater flow after plant trip. Although these operations are expected to occur and would result in a lower peak pressure, an additional degree of conservatism is provided by ignoring their effect.

The pressurizer safety valves start operating at 2500 psia and relieve steam at their rated capacities. Additional sensitivity analyses were performed at pressurizer safety valve settings of +6% and –4% of the nominal setpoint to account for the effects of steam accumulation and setpoint drift. The critical safety parameters were shown to be acceptable under these assumptions.

Calculations of the extent of DNB in the core during the accident are performed using the heat flux, the coolant flow decay and the coolant pressure and temperature as transient forcing functions.

In order to estimate the severity of the accident in the core as far as the integrity of the fuel rods is concerned the thermal behavior of the fuel located at the hot spot after DNB was investigated. Results obtained from an analysis of this "hot spot" condition represent the upper limit with respect to clad temperature, clad melting and zirconium-steam reaction.

Results

The coolant flow through the core is rapidly reduced to < 50% of its initial value (see Figure 14.1.8-9).

The reactor coolant pressure vs. time for a locked rotor accident is shown in Figure 14.1.8-11. The minimum DNBR for a fuel rod having an initial $F_{\Delta H}$ value of 1.470 is shown in Figure 14.1.8-12. The 1.470 $F_{\Delta H}$ rod reaches a MDNBR of slightly above the MDNBR limit. The MDNBR for the 1.70 $F_{\Delta H}$ fuel rod is less than the MDNBR limit, and the fuel rod is assumed to fail. Up to 40% of the fuel rods in the core can go below the MDNBR limit with acceptable radiological consequences (Reference 8). Fuel rod power census curves are generated for each reload to assess the percentage of fuel rods that are expected to go below the MDNBR limit of this accident.

Figure 14.1.8-13 shows the clad temperature transient at the hot spot. Since in the worst case examined, the clad temperature does not exceed 1800°F, it is not necessary to consider the possibility of a zirconium-steam reaction. The zirconium-steam reaction is only significant above this temperature.

The following table shows the comparison of the important calculated safety parameters to their respective acceptance criteria (Calculated Valve/Acceptance Criterion):

	% Fuel Rods < DNB Limit	Max Clad Temp. (°F)	RCS Pressure (psia)	MS Pressure (psia)
Locked Rotor	< 40*	1497/2700	2359/2750	1044/1210

* Percentage of Fuel Rods with $F_{\Delta H} \geq 1.470$

Conclusions

Since the peak pressure reached during the transient is < 110% of design, the integrity of the Reactor Coolant System is not endangered. The pressure can be considered as an upper limit because of the following conservative assumptions used in the study:

1. Credit is not taken for the negative moderator coefficient.

2. It is assumed that the pressurizer relief valves were inoperative.

3. The steam dump is assumed to be inoperative.

The peak clad temperature calculated for the hot spot, can also be considered an upper limit because of the following:

1. The hot spot is assumed to be in DNB at the start of the accident.

2. A high gap coefficient is used during the transient.

3. The nuclear heat released in the fuel at the hot spot is based on a zero moderator coefficient.

14.1.9 LOSS OF EXTERNAL ELECTRICAL LOAD

Accident Description

The loss of external electrical load may result from an abnormal increase in network frequency, opening of the main breaker from the generator, which causes a rapid large Nuclear Steam Supply System load reduction by the action of the turbine control, or by a trip of the turbine generator.

The plant is designed to accept a full-load rejection without actuating a reactor trip. The automatic steam dump system with 85% steam dump capacity (40% to the condenser and 45%

to the atmosphere) is able to accommodate this load rejection by reducing the transient imposed upon the Reactor Coolant System. The reactor power is reduced to the new equilibrium power level at a rate consistent with the capability of the Rod Control System. The pressurizer relief valves may be actuated, but the pressurizer safety valves and the steam generator safety valves do not lift for a step loss of load with steam dump to auxiliary load.

In the event the steam dump valves fail to open following a large load loss, the steam generator safety valves may lift and the reactor may be tripped by the high pressurizer pressure signal or the high pressurizer level signal. The steam generator shell side pressure and reactor coolant temperatures would increase rapidly. The pressurizer safety valves and steam generator safety valves are, however, sized to protect the Reactor Coolant System and steam generator against overpressure for all load losses without taking credit for the steam dump system.

The most likely source of a complete loss of load on the Nuclear Steam Supply System is a trip of the turbine generator. In this case, there is a direct reactor trip signal (unless below approximately 10% power) derived from either the turbine auto-stop oil pressure or a closure of the turbine stop valves. Reactor coolant temperatures and pressure do not significantly increase if the steam dump and pressurizer pressure control system are functioning properly. However, in this analysis, the behavior of the plant is evaluated for a complete loss of load from 102% of full power without a direct reactor trip primarily to show the adequacy of the pressure relieving devices and also to show that no core damage occurs. The Reactor Coolant System and Steam System pressure relieving capacities are designed to insure safety of the plant without requiring the automatic rod control, pressurizer pressure control and/or steam dump control systems.

Method of Analysis

The total loss of load transients are analyzed by employing a detailed digital computer program. The program describes the neutron kinetics, Reactor Coolant System, pressurizer, pressurizer relief and safety valves, pressurizer spray, steam generator, and steam generator safety valves.

The objectives of this analysis are to demonstrate margins to core protection limits and to demonstrate the adequacy of the plant pressure relieving devices.

a. The initial reactor power and Reactor Coolant System temperatures are assumed at their maximum values consistent with steady-state full power operation, including allowances for calibration and instrument errors. The initial Reactor Coolant System pressure is assumed at the minimum value consistent with steady-state full power operation, including allowances for calibration and instrument errors. This results in the maximum power difference for the load loss, and the minimum margin to core protection limits at the initiation of the total loss-of-load accident.

b. The total loss of load is analyzed for both BOC and EOC conditions. At BOC, a zero moderator coefficient (0.0 $\Delta k/°F$) is used; and at EOC, a moderator coefficient value of

–4.0E–4 Δk/°F is used. A conservatively large absolute value of the Doppler coefficient is used for all cases with a negative moderator coefficient. For the cases in which the moderator coefficient is zero, a conservatively small absolute value of the Doppler coefficient is used.

c. Two cases for both the beginning and end-of-life are analyzed as follows:

- The reactor is assumed to be in normal automatic control with the control rods in the minimum incremental worth region.
- The reactor is assumed to be in manual control with no control rod insertion until a reactor trip occurs.

d. No credit is taken for any of the steam dump valves or power-operated steam generator relief valves. The steam generator pressure rises to the safety valve set point where steam release through safety valves limits secondary steam pressure at the set point.

e. Two cases for both the beginning and end-of-life are analyzed as follows:

- Full credit is taken for the effect of pressurizer spray and power-operated relief valves in reducing or limiting coolant pressure.
- No credit is taken for the effect of pressurizer spray and power-operated relief valves in reducing or limiting coolant pressure.

A nominal pressurizer safety valve setpoint of 2500 psia is assumed. Sensitivity analyses were performed at pressurizer safety valve settings of +6% and –4% of the nominal setpoint to account for the effects of steam accumulation and setpoint drift. The critical safety parameters were shown to be acceptable under these assumptions.

Results

Figures 14.1.9-1 through 14.1.9-5 show the transient responses for a total loss of load at beginning of cycle with zero moderator temperature coefficient assuming full credit for the pressurizer spray, pressurizer power-operated relief valves, and automatic control rod insertion. No credit is taken for the steam dump system.

Figures 14.1.9-6 through 14.1.9-10 show the responses for the total loss of load at end of cycle with the most negative moderator temperature coefficient (–4.0E–4 Δk/°F). The rest of the plant operating conditions are the same as the case above.

The loss-of-load accident is also analyzed assuming manual RCCA control. In addition, no credit is taken for the pressurizer spray, pressurizer power-operated relief valves, or steam dump system. Figures 14.1.9-11 through 14.1.9-15 show the manual control beginning of cycle transient with zero moderator coefficient. Figures 14.1.9-16 through 14.1.9-20 show the manual control transient results at end of cycle.

The following table shows the comparison of the important calculated safety parameters to their respective acceptance criteria (Calculated Value/Acceptance Criterion):

Loss of Load	MDNBR	RCS Pressure (psia)	MSS Pressure (psia)
BOC Manual Control	1.681/1.14	2501/2750	1182/1210
BOC Auto Control	1.681/1.14	2474/2750	1184/1210
EOC Manual Control	1.681/1.14	2481/2750	1182/1210
EOC Auto Control	1.681/1.14	2377/2750	1198/1210

Conclusions

The safety analysis indicates that a total loss of load without a direct or immediate reactor trip presents no hazard to the integrity of the Reactor Coolant System or the Steam System. Pressure relieving devices incorporated in the two systems are adequate to limit the maximum pressures to within safety analysis limits. The integrity of the core is maintained by the Reactor Protection System. The MDNBR does not fall below its initial value, which is above the MDNBR limit.

14.1.10 LOSS OF NORMAL FEEDWATER

Accident Description

A loss of normal feedwater (from a pipe break, pump failure, valve malfunctions, or loss of off-site power) results in a reduction in capability of the secondary system to remove the heat generated in the reactor core. If the reactor is not tripped during this accident, Reactor Coolant System damage could possibly occur from a sudden loss of heat sink. If an alternative supply of feedwater is not supplied to the plant, residual heat following reactor trip heats the coolant to the point where water relief from the pressurizer occurs. Significant loss of water from the Reactor Coolant System could conceivably lead to core damage.

The following provides the necessary protection against a loss of normal feedwater:

1. Reactor trip on Low-Low water level in either steam generator.

2. Reactor trip on steam flow-feedwater flow mismatch in coincidence with low water level in either steam generator.

3. Two motor driven auxiliary feedwater pumps which are started automatically on:

   a) Low-Low level in either steam generator, or

   b) Opening of both feedwater pump circuit breakers, or

   c) Safety Injection signal, or

d) Loss of off-site power, or

e) Steam generator AMSAC low-low level, or

f) Manually

4. One turbine driven pump which is started automatically on:

   a) Low-Low level in both steam generators, or

   b) Loss of voltage on both 4 kV. buses, or

   c) Steam generator AMSAC low-low level, or

   d) Manually

The motor-driven auxiliary feedwater pumps are supplied power by the diesel generators if a loss of outside power occurs. The turbine-driven pump uses steam from the secondary system. The turbine exhausts the secondary steam to the atmosphere. The auxiliary feedwater pumps take suction directly from the condensate storage tank for delivery to the steam generators.

Three auxiliary feedwater pumps are provided in the plant (two motor driven and one turbine driven). Necessary protection against consequences of a loss of normal feedwater including that caused by loss of off-site power is therefore available, allowing for an active failure on one of the operable auxiliary feedwater pumps even when one of the pumps is out-of-service.

When all three pumps are operable there is considerable backup in equipment and control to insure that reactor trip and automatic auxiliary feedwater flow occur following loss of normal feedwater.

Method of Analysis

The analysis was performed using a digital simulation of the plant to show that following a loss of normal feedwater, the Auxiliary Feedwater System is adequate to remove stored and residual heat.

The following assumptions are made:

1. The initial steam generator water level (in both steam generators) when the reactor trip occurs is assumed to be at the Low-Low level tap. This is conservative, because this level would result in a reactor trip and automatic initiation of the auxiliary feedwater flow.

2. The plant is initially operating at 102% of 1650 MWt.

3. Off-Site power is not available, resulting in natural circulation flow in the Reactor Coolant System.

4. A conservative core residual heat generation based upon long-term operation at the initial power level preceding the trip.

5. Only one motor-driven auxiliary feedwater (AFW) pump is available 630 seconds after the accident is initiated.

6. Auxiliary feedwater is delivered to only one steam generator, at a flow rate of 176 gpm.

7. Secondary system steam relief is through the self-actuated safety valves. Nominal safety valve settings and rated safety valve flow capacities (Section 10.2.2) are assumed. To maximize the pressurizer insurge and Reactor Coolant System heatup, the safety valve blowdown settings are assumed to be at 0%. (Steam relief would in fact, be through the power-operated relief valves or condenser dump valves for most cases of loss of normal feedwater. However, these are assumed to be unavailable in the analysis).

Results

Figures 14.1.10-1 through 14.1.10-5 show the plant parameters following a loss of normal feedwater accident with the assumptions listed above. Following the reactor and turbine trip from full load, the water level in the steam generators falls due to the reduction of steam generator void fraction and because steam flow through the safety valves continues to dissipate the stored and generated heat. The auxiliary feedwater pump is delivering flow 630 seconds following the initiation of the low-low level trip, thus reducing the rate of water level decrease. The capacity of the auxiliary feedwater pump is such that the water level in the steam generator being fed does not recede below the lowest level at which sufficient heat transfer area is available to dissipate core residual heat without water relief from the primary system relief or safety valves.

From Figures 14.1.10-1 through 14.1.10-5, it can be seen that at no time is the tube sheet uncovered in the steam generator receiving auxiliary feedwater flow and at no time is there water relief from the pressurizer. If the auxiliary feed delivered is greater than that of one motor driven pump, the initial reactor power is < 102% of 1650 MWt, or the steam generator water level in one or both steam generators is above the Low-Low level trip point at the time of trip, then the result is a steam generator minimum water level higher than shown and an increased margin to the point at which reactor coolant water relief occurs.

The following table shows the comparison of the important calculated safety parameters (Calculated Value/Acceptance Criterion):

	MDNBR	RCS Pressure (psia)	MS Pressure (psia)
Loss of Feedwater	1.681/1.14	2500/2750	1165/1210

Conclusion

The loss of normal feedwater does not result in any adverse condition in the core, because it does not result in water relief from the pressurizer relief or safety valves, nor does it result in uncovering the tube sheets of the steam generator being supplied with water.

14.1.11 ANTICIPATED TRANSIENTS WITHOUT SCRAM

An Anticipated Transient Without Scram (ATWS) is a postulated anticipated operational occurrence (such as loss of feedwater, loss of condenser vacuum, or loss of off-site power) that is accompanied by a failure of the Reactor Protection System (RPS) to shut down the reactor.

The Kewaunee Nuclear Power Plant was originally licensed based on the results of a study of ATWS presented in WCAP 7486 (see Reference 9). The conclusions of this study are that there is very little likelihood of failure to trip the reactor and that even in the hypothetical case of no protective reactor trip, there is no gross fuel damage. WCAP 8330 presented the results of generic ATWS analysis for 2, 3, and 4 loop Westinghouse plants. The results of these analyses showed that the consequences of an ATWS were acceptable as long as the turbine was tripped and AFW initiated in a timely fashion. Acceptable consequences are defined as RCS pressure remaining below 3200 psig and no fuel failure. The results of the analyses in WCAP 8330 also showed that the most severe ATWS transients were those which entailed a loss of main feedwater. Subsequent to the operational license at KNPP and based on the studies cited above, additional ATWS protection was required as described below.

The Code of Federal Regulations (CFR), Section 10 CFR 50.62 (Reference 10) specifies ATWS mitigation system requirements. The Westinghouse Owners Group developed a set of conceptual ATWS Mitigating System Actuation Circuitry (AMSAC) designs (Reference 11). The AMSAC actuation on low steam generator water level design has been implemented, with the exception that AMSAC is armed at all power levels (the "c-20 permissive" signal is not used). The logic of AMSAC is to trip the turbine and start all three auxiliary feedwater pumps when low-low steam generator water level signals are present on 3 of 4 channels for a specified time period. However, as discussed in Section 6.6, manual initiation of auxiliary feedwater may be required at low power levels (< 15%). The level setpoint and time delay criteria are described in Reference 11.

The NRC Safety Evaluation Report (Reference 12) and a subsequent NRC Special Inspection Report (Reference 13) reviewed the Kewaunee design and installation against 14 key elements for compliance. The NRC concluded that the Kewaunee AMSAC is acceptable and in compliance with the ATWS rule, 10 CFR 50.62.

In 1998, in response to an engineering evaluation of the AFW system, a plant design change added a Diverse Scram System (DSS). The DSS is initiated on a signal from the existing AMSAC system and de-energizes the Rod Drive MG Set exciter field. Removing the Rod Drive MG set exciter field will interrupt power to the control rod grippers, allowing the control rods to free fall into the core, ending the ATWS event.

The DSS was installed to ensure the AFW pumps would continue to run throughout a loss of main feedwater ATWS. The DSS in conjunction with the AMSAC system will end the transient before the AFW flow to the steam generators increases to a point where AFW pump NPSH could be lost. The loss of main feedwater ATWS, mitigated by the DSS and AMSAC system, was analyzed using a similar methodology as the loss of main feedwater transient described in Section 14.1.10.

The original AMSAC submittal to the NRC was amended to include the DSS. The NRC Safety Evaluation Report (Reference 15) concluded that the Kewaunee DSS design was acceptable. The WPSC Safety Evaluation for the original AMSAC and the DSS included a review of the 14 key elements of ATWS compliance used by the NRC. This review concluded that the original AMSAC design reviewed by the NRC was unaffected by the addition of the DSS.

14.1.12 LOSS OF AC POWER TO THE PLANT AUXILIARIES

In the event of a complete loss of off-site power and a turbine trip, there will be a loss of power to the plant auxiliaries, i.e., the reactor coolant pumps, main feedwater pumps, etc. The events following a loss of off-site power with turbine trip are described in the sequence below.

a. The reactor is tripped and plant vital instruments are supplied by the emergency power sources.

b. The diesel generators start on loss-of-voltage on the 4kV buses to supply plant vital loads.

c. As the steam system pressure subsequently increases, the steam system power-operated relief valves are automatically opened to the atmosphere. Steam dump to the condenser is assumed not available because of loss of the circulating water pumps.

d. If the steam flow rate through the power-operated relief valves is not sufficient (or if the power-operated relief valves are not available), the steam generator self-actuated safety valves may lift to dissipate the sensible heat of the fuel and coolant plus the residual heat produced in the reactor.

e. As the no-load temperature is approached, the steam power-operated relief valves (or self-actuated safety valves if the power-operated relief valves are not available for any reason) are used to dissipate the residual heat and to maintain the plant at the hot shutdown condition.

The auxiliary feedwater system is started automatically on loss of off-site power. The turbine-driven auxiliary feedwater pump uses steam from the secondary system and exhausts to the atmosphere. The motor-driven auxiliary feedwater pumps are supplied by power from the diesel generators. The pumps take suction directly from the condensate storage tank for delivery to the steam generators. The auxiliary feedwater system ensures feedwater flow upon loss of power to the plant auxiliaries. The flow rate assumed in the safety analysis for each of

the two motor-driven auxiliary feedwater pumps and the turbine-driven auxiliary feedwater pump is the same as that discussed in Section 14.1.1.10.

The turbine-driven pump can be tested at any time by admitting steam to the turbine driver. The motor-driven pumps also can be tested at any time. The auxiliary feedwater control valves can be operationally tested whenever the plant is at hot shutdown and the remaining valves in the system are operationally tested when the pumps are tested.

Upon the loss of power to the reactor coolant pumps, coolant flow necessary for core cooling and the removal of residual heat is maintained by natural circulation in the reactor coolant loops. The natural circulation flow was calculated for the conditions of equilibrium flow and maximum loop-flow impedance.

In response to Generic Letter 81-21, the ability to cool down via natural circulation without voiding the upper head of the reactor vessel was reviewed. The NRC concluded in Reference 14 that Kewaunee has adequately demonstrated the ability to cooldown without voiding the reactor vessel head and determined that sufficient condensate supply exists to support its cooldown procedures.

The average temperature, pressurizer water volume, and steam generator level assuming the most conservative initial plant conditions and equipment availability are shown in Figures 14.1.10-1, 14.1.10-2, 14.1.10-3, and 14.1.10-4 for a loss of normal feedwater including a loss of off-site power, and reactor coolant system natural circulation. It is shown in Section 14.1.10 that a loss of normal feedwater from any cause including a loss of off-site power does not result in water relief from the pressurizer relief or safety valves.

Conclusion

The loss of off-site power to the plant auxiliaries does not cause any adverse condition in the core since it does not result in water relief from the pressurizer relief or safety valves nor does it result in the loss of the steam generator(s) as a heat sink for residual heat removal.

REFERENCES - SECTION 14.1

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10. 10CFR50.62, Requirements for reduction of risk from anticipated transients without scram (ATWS) events for light-water-cooled nuclear power plants

11. MR Adler, "AMSAC Generic Design Package" WCAP-10858P-A, Revision 1, July 1987

12. NRC Safety Evaluation Report, JG Giitter (NRC) to DC Hintz (WPS) Letter No. K-88-202 dated August 25, 1988

13. NRC Special Inspection Report, RW Cooper II (NRC) to KH Evers (WPS), Letter No. K-89-209 dated October 25, 1989

14. NRC Safety Evaluation Report, SA Varga (NRC) to CW Giesler (WPS), Letter No. K-83-170 dated August 15, 1983

15. Letter from WO Long (NRC) to ML Marchi (WPSC), transmitting the NRC SER approving the AMSAC Modification, Letter No. K-98-101 dated July 19,1998

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14.2 STANDBY SAFETY FEATURES ANALYSIS

Adequate provisions have been included in the design of the plant, and its standby engineered safeguards to limit potential exposure of the public to below the guidelines of 10 CFR 100 for situations which have a very low probability of occurrence, but which could conceivably involve uncontrolled releases of radioactive materials to the environment. The situations, which have been considered, are:

- Fuel Handling Accidents
- Accidental Release of Waste Liquid
- Accidental Release of Waste Gases
- Rupture of a Steam Generator Tube
- Steam Line Break
- Rupture of a Control Rod Drive Mechanism Housing - RCCA Ejection
- Turbine Missile Damage to Spent Fuel Pool

14.2.1 FUEL HANDLING ACCIDENTS

The following fuel-handling accidents are evaluated to ensure that no hazards are created:

a. A fuel assembly becomes stuck inside the reactor vessel

b. A fuel assembly or Rod Cluster Control Assembly (RCCA) is dropped onto the floor of the reactor refueling cavity or spent fuel pool

c. A fuel assembly becomes stuck in the penetration valve

d. A fuel assembly becomes stuck in the transfer tube or the carriage becomes stuck.

Causes and Assumptions

The possibility of a fuel handling incident of the severity considered in the analysis is very remote because of the many administrative controls and physical limitations imposed on fuel handling operations. All refueling operations are conducted in accordance with prescribed procedures under direct surveillance of a supervisor technically trained in nuclear safety. Also, before any refueling operations begin, verification of complete RCCA insertion is obtained by weighing each control rod drive mechanism individually to verify that the control rods are disengaged from the control rod drive mechanisms. Boron concentration in the coolant is raised to the refueling concentration and verified by sampling. Refueling boron concentration is sufficient to maintain the clean, cold, fully loaded core sub-critical with all RCCAs withdrawn. The refueling cavity is filled with water meeting the same boric acid specifications.

As the vessel head is removed, a visual check is made to verify that RCCA drive shafts are free of the mechanism housings.

After the vessel head is removed, the RCCA drive shafts are disconnected from their respective assemblies using the manipulator crane and the shaft-unlatching tool. A spring scale is used to indicate that the drive shaft is free of the RCCA as the lifting force is applied.

The fuel handling manipulators and hoists are designed so that fuel cannot be raised above a position which provides adequate shield water depth for the safety of operating personnel. This safety feature applies to handling facilities in both the containment and in the spent fuel pool area. In the spent fuel pool, the design of storage racks and manipulation facilities is such that:

Fuel at rest is positioned by positive restraints in a safe, always sub-critical, geometrical array, with no credit for boric acid in the water.

Fuel can be manipulated only one assembly at a time.

Violation of procedures, by placing one fuel assembly in juxtaposition with any group of assemblies in racks does not result in criticality.

Crane facilities do not permit the handling of heavy objects, such as a spent fuel-shipping container, over the spent fuel storage area. A detailed description of crane movement limitations appears in Section 9.5.

Adequate cooling of fuel during underwater handling is provided by convective heat transfer to the surrounding water. The fuel assembly is immersed continuously while in the Refueling Cavity or Spent Fuel Pool.

Even if a spent fuel assembly becomes stuck in the transfer tube, the fuel assembly is completely immersed and natural convection maintains adequate cooling to remove the decay heat. The fuel handling equipment is described in detail in Section 9.5.

Two Nuclear Instrumentation System source-range channels are continuously in operation and provide warning of any approach to criticality during refueling operations. This instrumentation provides a continuous audible signal in the containment, and would annunciate a local horn and an annunciator in the plant control room if the count rate increases above a preset low level.

Refueling boron concentration is sufficient to maintain the clean, cold, fully loaded core sub-critical by at least 5% $\Delta k/k$ with all RCCAs inserted. At this boron concentration, the core would also be more than 2% sub-critical with all control rods withdrawn.

All these safety features make the probability of a fuel-handling incident very low. Nevertheless, it is possible that a fuel assembly could be dropped during the handling operations. Therefore, this incident is analyzed both from the standpoint of radiation exposure and accidental criticality.

Special precautions are taken in all fuel handling operations to minimize the possibility of damage to fuel assemblies during transport to and from the spent fuel pool and during installation in the reactor. All handling operations of irradiated fuel are conducted under

water. The handling tools used in the fuel handling operations are conservatively designed and the associated devices are of a fail-safe design.

In the fuel storage area, the fuel assemblies are spaced in a pattern that prevents any possibility of a criticality accident.

The motions of the cranes, which move the fuel assemblies, are limited to a low maximum speed. Caution is exercised during fuel handling to prevent the fuel assembly from striking another fuel assembly or structures in the containment or fuel storage building.

The fuel handling equipment suspends the fuel assembly in the vertical position during fuel movements, except when the fuel is moved through the transport tube.

The design of the fuel assembly is such that the fuel rods are restrained by grid clips which provide a total restraining force of approximately 40 pounds on each fuel rod at the end of life. The force transmitted to the fuel rods during normal handling is limited to the (grid frictional) restraining force and is not sufficient to breach the fuel rod cladding. If the fuel rods are not in contact with the fuel assembly bottom nozzles, the rods would have to slide against the 40-pound friction force. This would dissipate an appreciable amount of energy and thus limit the impact force on the individual fuel rods.

If one assembly is lowered on top of another, no damage to the fuel rods would occur that would breach the cladding. Considerable deformation would have to occur before the fuel rods would contact the top nozzle adapter plate and apply any appreciable load to the rods. Based on the above, it is unlikely that any damage would occur to the individual fuel rods during handling.

If during handling and subsequent translational motion the fuel assembly should strike against a flat surface, the fuel assembly lateral loads would be distributed axially along its length with reaction forces at the grid clips and essentially no damage would be expected in any fuel rods.

Analyses have been made assuming that fuel assembly is dropped vertically and strikes a rigid surface and where one fuel assembly is dropped vertically on another. The analysis of a dropped fuel assembly striking a rigid surface considers the stresses in the fuel cladding and any possible buckling of the fuel rods between the grid supports. The results show that the buckling load at the bottom section of the fuel rod, which would receive the highest loading, is below the critical buckling load and the stresses are below the yield stress. For the case in which a fuel assembly is assumed to be dropped on top of another assembly, the impact load is transmitted through the top nozzle and the RCCA guide tubes of the struck assembly before any of the loads reach the fuel rods. As a result, a significant amount of kinetic energy is absorbed by the top nozzle of the struck assembly and bottom nozzle of the falling assembly, thereby limiting the energy available for fuel rod deformation. The results of this analysis indicated that the buckling load on the fuel rods is below the critical buckling load and stresses in the cladding are below yield.

Prototype fuel assemblies have been subjected to 3000 pounds of axial load without excessive lateral or axial deformation. The maximum column load expected to be experienced in service

is approximately 1000 pounds. This information is used in the fuel handling equipment design to establish the limits for inadvertent axial loads.

For the purposes of evaluating the environmental consequences of a fuel-handling incident, a conservative upper limit of damage is assumed by considering the cladding rupture of all rods in one complete fuel assembly. The remaining fuel assemblies are so protected by the storage rack structure that no lateral bending loads would be imposed.

Activity Release Characteristics

For the assumed accident, there is a sudden release of the gaseous fission products held in the gap between the pellets and cladding of one fuel assembly. The low temperature of the fuel during handling operations precludes further significant release of gases from the pellets themselves after the cladding is breached. Molecular halogen release is also greatly minimized due to their low volatility at these temperatures. The strong tendency for iodine in vapor and particulate form to be scrubbed out of gas bubbles during their ascent to the water surface further reduces the quantity released from the water surface.

The fuel assembly gap activity was conservatively calculated assuming an initial heavy metal loading of 411 kg of Uranium at 5 weight percent U-235. The plant is assumed to be operated at 1721 MW_{th}. Additionally, all of the rodlets in the assembly are assumed to be operated at a peak radial power ratio of 1.70 and a range of burnups up to 60 GWD/MTU. Activity levels corresponding to 48 GWD/MTU were identified as limiting. The iodine gap activities are assumed to be 12% of total fuel iodine activity. The Kr-85 gap activity is assumed to be 30% of the total fuel Kr-85 activity. All other noble gas gap activities are assumed to be 10% of the total fuel noble gas activity. The noble gas and iodine fission products calculated to be present in the fuel rod gap at 100 hours following shutdown are given in Table D.3-2 of Appendix D.

In examining the expected behavior of fission product halogens released from the damaged fuel cladding gap to the spent fuel pool or reactor cavity water, it was predicted that a significant portion of the halogens would be absorbed into the solution from the bubbles and fixed gases. Early experiments indicate that gaseous iodine (admixed with fixed gases) is readily transferred to the aqueous boric acid solution and that a high stripping efficiency results in a pool decontamination factor (DF) on the order of 10^3, i.e., only 1/1000 of the incident halogen reaches the pool surface and is available for release to the environment.

Studies have been performed by Westinghouse to confirm this stripping efficiency with laboratory tests that better represent the conditions of the assumed accident (Reference 1). An experimental arrangement, consisting of a 9-inch diameter vertical column containing boric acid solution at a depth of up to 7.5 feet, was utilized in the study. At the bottom of the column, a gas injection vessel was provided to permit the introduction of a fixed gas (nitrogen) containing iodine vapor at the design basis concentration. The gas mixture was injected in the solution and the stripping efficiency for the molecular iodine was determined by inventory of the fraction, which escaped from the aqueous solution, compared to the quantity retained in the solution. The bubble size was controlled, as was the solution depth, so that the relationship between DF and these variables could be determined.

The results of these studies indicate that the boric acid solution is an efficient getter for the iodine and that a DF for the 40-foot deep spent fuel pool is in excess of 500 for gas bubble diameters up to 1 inch.

As an extension of the laboratory studies, a full-scale fuel assembly mockup was fabricated to permit the examination of bubble diameters that would result from the damage of all fuel tubes in an assembly. The mockup simulated in exact detail the cross section of an assembly and was fabricated in a manner to permit the simultaneous release of gap gases from all fuel tubes into a deep (25 feet) water pool.

Close examination of the gas bubbles rising from the mockup demonstrates that, for the most part, the bubble diameters are near 1 inch and below. Some few exceptions are noted, resulting from coalescence of a fraction of the smaller bubbles giving rise to diameters larger than 1 inch, but only after several feet of bubble travel through the pool water. These large-scale tests confirm the high stripping efficiencies for the halogens and that DFs of greater than 500, as indicated by the quantitative laboratory-scale tests, are reasonably expected values.

Method of Analysis

The volatile gaseous activities associated with the fuel handling accident could be released either inside the Containment Building or in the Auxiliary Building. Both of these areas have ventilation systems in operation under administrative control during fuel handling operations. Radioactivity monitors provide continuous indication of radiation levels and signal evacuation of these areas on high alarm. The Containment Building high-level alarm automatically closes the purge supply and exhaust ducts. Administrative evaluation of the containment activity would determine when purging could be resumed. A high-level alarm on the Auxiliary Building Vent Monitor would automatically activate the Zone Special Ventilation (SV) System with subsequent absolute and charcoal filtration. This system is described in Section 9.

The fuel handling accident in containment with the personnel air lock doors open has been determined to be the limiting accident. In this analysis, all of the rods of one assembly (179 rodlets) are assumed to be damaged releasing the entire gap activity. Scrubbing of iodine by the borated water results in a decrease in the radioiodine activity available for release. A conservative value of 0.01 for scrubbing by the water is assumed. The activity released from the water surface mixes into the containment atmosphere. Following the containment ventilation system isolation, there is a limited driving force for a release to the environment even with the personnel air lock doors open. However, a conservative value for containment discharge of 6000 scfm is assumed. Additionally, a conservative value for containment volume is assumed to be 1.00E6 ft^3. Taking no credit for containment closure the release is assumed to continue for 2 hours at which point the release is stopped. Using these assumptions (i.e., volume and flow) a reduction of 0.5 is applied to the activity release.

Dispersion of this activity is computed using the Gaussian plume dispersion formula and taking credit for building wake dilution. A wind velocity of 1.5 meters per second is assumed to remain

in one direction for the duration of the accident under Pasquill F conditions. The dispersion characteristics are discussed in Section 2.7 and curves, corrected for building wake effect by the volumetric source method, are presented on Figure 2.7-5. The site boundary, Exclusion Area Boundary χ/Q, dispersion factor is 2.23E–4 sec/m^3. The Low Population Zone χ/Q is 3.977E–5 sec/m^{33}.

The dose to the thyroid has been determined to be limiting. With the ICRP 30 dose conversion factors applied and the above mentioned conservative inputs the thyroid doses at the site boundary and low population zone are 24.4 and 4.4 rem respectively.

Thus, it is concluded that a dropped fuel assembly would present no criticality hazard and would result in radiation levels at the site boundary and low population zone that are well below the 10 CFR 100 guidelines. This analysis was reviewed and approved by the NRC in Reference 2.

14.2.2 ACCIDENTAL RELEASE-RECYCLE OF WASTE LIQUID

Accidents in the Auxiliary Building that result in the release of radioactive liquids are those that involve the rupture or leaking of system pipe lines or storage tanks. The largest vessels are the three liquid holdup tanks, sized such that two tanks can hold more than one reactor coolant liquid volume, used to store the normal recycle or water fluids produced. The contents of one tank are passed through the liquid processing train while the other tanks are being filled.

All liquid waste components except the reactor coolant drain tank are located in the Auxiliary Building and any leakage from the tank or piping will be collected in the building sump to be pumped back into the liquid waste system. The building sump and basement volume are sufficient to hold the full volume of a liquid holding tank without overflowing to areas outside the building. This also is true for the tanks in the Auxiliary Building.

The holdup tanks are also equipped with safety pressure relief and designed to accept the established seismic forces at the site. Liquids in the Chemical and Volume Control System flowing into and out of these tanks are controlled by manual valve operation and governed by prescribed administrative procedures.

The volume control tank design philosophy is similar in many respects to that applied for the holdup tanks. Level alarms, pressure relief valves and automatic tank isolation and valve control assure that a safe condition is maintained during system operation. Excess letdown flow is directed to either the holdup tanks via the reactor coolant drain tank or the volume control tank. The waste holdup tank is a horizontal tank, which is continuously maintained at atmospheric pressure. Its vent is routed to the atmosphere through the Auxiliary Building exhaust ducts.

The potential hazard from these process or waste liquid releases is derived only from the volatilized components. The releases are described and their effects summarized in Section 14.2.3.

The evaluation of the credibility of the accidental release of radioactive fluids above maximum normal concentration (4E–5 μCi/cc) from the Waste Disposal System discharge is based upon the following review of waste discharge operating procedure, monitoring function description, monitor failure mode and the consequences of a monitor failure.

The process for discharging liquid wastes is as follows:

a. A batch of waste is collected in one Steam Generator Blowdown Treatment tank (capacity 10,000 gal); other lesser volume tank(s) can be used and follow the same process;

b. The tank, or tanks, is (are) isolated;

c. The tank(s) contents are recirculated to mix the liquid;

d. A sample is taken for radiochemical analysis;

e. If analysis indicates that release can be made within permissible limits, the quantity of activity to be released is recorded on the basis of the liquid volume in the tank(s) and its activity concentration. Each tank or batch is assessed for its radiological impact prior to and/or after each release. If release can not be made within permissible limits, the waste is returned for additional cleanup. Then the process begins again.

f. To release the liquid, the last stop valve in the discharge line (which is normally locked shut) must be unlocked and opened; a second valve, which trips shut automatically on high radiation signal from the effluent monitor, must be opened manually; a pump for the tank being released must be started manually and a flow rate established. The release flow rate is set at or below the maximum release flow rate as listed on the Radiological Liquid Waste Discharge Permit. Liquid is now being pumped to the discharge canal.

As the operating procedure indicates, the release of liquid waste is under administrative control. The effluent monitor is provided to maintain surveillance over the release.

The effluent monitor is provided with the following features:

a. A check source is provided to permit the operator to check the operation of the monitor before discharge from the control room.

b. If the monitor falls off scale at any time, an alarm condition is indicated in the control room and the waste disposal discharge valve is tripped closed automatically.

c. If the AC power supply to the monitor fails, a high radiation alarm is annunciated. The trip valve also closes.

d. The normally closed radiation trip valve fails closed.

It is concluded that the administrative controls imposed on the operator combined with the safety features built into the equipment provide a high degree of assurance against accidental release of waste liquids.

Should a complete failure of any tank located in the Auxiliary Building occur, its contents remains in this building. Any subsequent discharge of radioactive liquid to the lake is be conducted under the controls described above and does not result in activity concentrations in excess of the limits given in the Off-Site Dose Calculation Manual (ODCM).

Dilution of off-site liquid releases is discussed in Section 2.6.4.

14.2.3 ACCIDENTAL RELEASE-WASTE GAS

Gas Decay Tank Rupture

Causes and Assumptions

The gas decay tanks contain the gases vented from the Reactor Coolant System the volume control tank, and the liquid holdup tanks. Sufficient volume is provided in each of four tanks to store the gases evolved during a reactor shutdown. The system is adequately sized to permit storage of these gases for forty-five days prior to discharge.

This period is selected as the maximum foreseeable holdup time because in this period the shorter-lived radioactive gaseous isotopes received by the waste system will have decayed to a level, which is less significant than that of long-lived Kr^{85}.

The waste gas accident is defined as an unexpected and uncontrolled release to the atmosphere of the radioactive xenon and krypton fission gases that are stored in the waste gas storage system. Failure of a gas decay tank or associated piping could result in a release of this gaseous activity. This analysis shows that even with the worst expected conditions, the off-site doses following release of this gaseous activity would be very low.

The leakage of fission products through cladding defects can result in a buildup of radioactive gases in the reactor coolant. Based on experience with other operational closed cycle, pressurized water reactors, the number of defective fuel elements and the gaseous activity in the coolant is expected to be low. The principal source of radioactive gases in the Waste Disposal System is the bleeding of effluents from the Reactor Coolant System.

Nonvolatile fission product concentrations are greatly reduced as the cooled Reactor Coolant System liquid is passed through the purification demineralizers. (The removal factor for iodine, for example, is at least 10). The decontamination factor for iodine between the liquid and vapor phases, for example, is expected to be on the order of 10,000. Based on the above analysis and operating experience at Yankee-Rowe and Saxton, activity stored in a gas decay tank consists of the noble gases released from the processed coolant with only negligible quantities of the less volatile isotopes.

The components of the waste gas system are not subjected to any high pressures or stresses, are Class I design (see Appendix B), and are designed to the standards given in Table 11.1-2. A rupture or failure is highly unlikely. However, a rupture of a gas decay tank was analyzed to define the hazard caused by a malfunction in the radioactive waste disposal system.

Activity Release Characteristics

The activity in a gas decay tank is taken to be the maximum amount that could accumulate from operation with cladding defects in 1% of the fuel elements. This is at least ten times the expected number of defective fuel elements. The maximum activity is obtained by assuming the noble gases, xenon and krypton, are accumulated with no release over a full core cycle. This postulated amount of activity, one Reactor Coolant System equilibrium cycle inventory, is given in Appendix D. This value is particularly conservative because some of this activity would normally remain in the coolant, some would have been dispersed earlier through the vent, and the shorter-lived isotopes would have decayed substantially.

Samples taken from gas storage tanks in pressurized water reactor plants in operation show no appreciable amount of iodine.

To define the maximum doses, the release is assumed to result from gross failure of any process system storage tank, here represented by a gas decay tank giving an instantaneous release of its volatile and gaseous contents to the atmosphere.

Volume Control Tank Rupture

Causes and Assumptions

The volume control tank contains fission gases and low concentrations of halogens, which are normally a source of waste gas activity, vented to a gas decay tank. The iodine concentrations and volatility are quite low at the temperature, pH, and pressure of the fluid in the volume control tank. The same assumptions detailed in the preceding subsection apply to this tank. As the volume control tank and associated piping are not subjected to any high pressures or stresses, failure is very unlikely. However, a rupture of the volume control tank is analyzed to define the limit of the exposure that could result from such an occurrence.

Activity Release Characteristics

Rupture of the volume control tank is assumed to release all the contained noble gases and 1% of the halogen inventory of the tank plus that amount contained in the 40-gpm flow from the demineralizers, which would continue for up to five minutes before isolation would occur. The 1% halogen release is a very conservative estimate of the decontamination factor expected for these conditions.

Based on 1% fuel defects, the activities available for release are given in Table D.6-1.

Method of Analysis

In calculating off-site plume centerline exposure, it is assumed that the activity is discharged to the atmosphere at ground level and is dispersed as a Gaussian plume downwind taking into account building wake dilution.

No credit is taken for the buoyant lift effect of the hydrogen present in the released gas. Dispersion coefficients based on the on-site meteorology program are used. A wind velocity of 1.5 meters per second is assumed to remain in one direction for the duration of the accident under Pasquill F conditions. The dispersion characteristics are discussed in Section 2.7.4 and curves corrected for building wake effects by the volumetric source method, are present on Figure 2.7-8.

The following parameters have been used in the dose assessment:

- A 0-2 hour χ/Q value of 2.23E–4 sec/m^3
- Breathing rate equal to 3.47E–4 m^3/sec
- The effective decay energies for noble gases found in Table D.8-1.
- The ICRP 2 thyroid dose conversion factors for iodine inhalation.
- The volume control tank specific activities are found in Table D.6-1.
- The gas decay tank activities are found in Table D.7-1.

Summary of Calculated Doses

The following tabulation summarizes the whole body and thyroid doses at the site boundary (exclusion distance), consistent with a receptor on the plume centerline.

	Thyroid Dose	Whole Body Dose
Gas Decay Tank Rupture	Negligible	0.327 rem
Volume Control Tank Rupture	1.06E–3 rem	0.082 rem
10 CFR 100 Guidelines	300 rem	25 rem

It is concluded that a rupture in the waste gas system or in the volume control tank would present no undue hazard to public health and safety.

14.2.4 STEAM GENERATOR TUBE RUPTURE

Accident Description

The accident examined is the complete severance of a single steam generator tube (for additional information on steam generator tubes, see Sections 14.3.10 and 14.3.11), with the reactor at power. This accident leads to an increase in contamination of the secondary system due to leakage of radioactive coolant from the Reactor Coolant System. In the event of a coincident loss of off-site power, or failure of the condenser dump system, discharge of activity to the atmosphere takes place via the steam generator safety and/or power operated relief valves.

The activity that is available for release from the system is limited by:

1. Activities in the steam generator secondary that are a consequence of operational leakage prior to the complete tube rupture.

2. The activity concentration in the reactor coolant, which is conservatively assumed to arise from 1% defective fuel clad.

3. Operator actions to isolate the mixed primary and secondary leakage to atmosphere.

The steam generator tube material is Inconel 600 and, as the material is highly ductile, it is considered that the assumption of a complete severance is conservative. The more probable mode of tube failure would be one or more minor leaks of undetermined origin. Activity in the Steam and Power Conversion System is subject to continuous surveillance and an accumulation of minor leaks that cause the activity to exceed the limits established in the Technical Specifications is not permitted during reactor operation.

The operator determines that a steam generator tube rupture has occurred, and identifies and isolates the faulty steam generator on a restricted time scale in order to minimize contamination of the secondary system and ensure termination of radioactive release to the atmosphere from the faulty unit. The recovery procedure is carried out on a time scale that ensures that break flow to the secondary system is terminated before water level in the faulty steam generator rises into the main steam line. Sufficient indications and controls are provided to enable the operator to carry out these functions satisfactorily. Consideration of the indications provided on the control board together with the magnitude of the break flow leads to the conclusion that the isolation procedure can be completed within thirty minutes of accident initiation.

Assuming normal operation of the various plant control systems, the following sequence of events is initiated by a tube rupture:

1. Pressurizer low-pressure and low-level alarms are actuated and, prior to plant trip, charging pump flow increases in an attempt to maintain pressurizer level. On the secondary side there is a steam flow/feedwater flow mismatch before trip, as feedwater flow to the faulty steam generator is reduced due to the additional break flow which is now being supplied to that generator.

2. Loss of reactor coolant inventory leads to falling pressure and level in the pressurizer until a reactor trip signal is generated by low pressurizer pressure. Resultant plant cooldown following reactor trip leads to a rapid change of pressurizer level, and the safety injection signal, initiated by low pressurizer pressure, follows soon after the reactor trip. The safety injection signal automatically terminates normal feedwater supply and initiates auxiliary feedwater addition; as discussed in Section 6.6; manual initiation of auxiliary feedwater may be required at low power levels.

3. The steam generator blowdown liquid monitor and the air-ejector radiation monitor will alarm, indicating a sharp increase in radioactivity in the secondary system.

4. The plant trip automatically shuts off steam supply to the turbine and if off-site power is available the condenser steam dump valves open permitting steam dump to the condenser. In the event of a coincident loss of off-site power, the condenser steam dump valves automatically close to protect the condenser. The steam generator pressure rapidly increases resulting in steam discharge to the atmosphere through the steam generator safety and/or power-operated relief valves.

5. Following plant trip, the continued action of auxiliary feedwater supply and borated safety injection flow (supplied from the Refueling Water Storage Tank) provide a heat sink, which absorbs some of the decay heat. Thus, steam bypass to the condenser, or in the case of loss of off-site power, steam relief to atmosphere, is attenuated during the thirty minutes in which the recovery procedure leading to isolation is being carried out.

6. Safety injection flow results in increasing pressurizer water level. The time after trip at which the operator can clearly see returning level in the pressurizer is dependent upon the amount of operating auxiliary equipment.

Results

In determining the mass transfer from the Reactor Coolant System through the broken tube, several conservative assumptions are made as follows:

a. Plant trip occurs automatically as a result of low pressurizer pressure.

b. Following the initiation of the Safety Injection Signal, both Safety Injection Pumps are actuated and continue to deliver flow for thirty minutes.

c. After plant trip the break flow equilibrates at the point where incoming safety injection flow is balanced by outgoing break flow as shown in Figure 14.2.4-1. The resultant break flow persists from plant trip until thirty minutes after the accident.

d. The steam generators are controlled at the safety valve setting rather than the power-operated relief valve setting.

e. The operator identifies the accident type and terminates break flow to the faulty steam generator within thirty minutes of accident initiation.

The above assumptions lead to a conservative estimate of 120,000 lbs. for the total amount of reactor coolant transferred to the faulty steam generator as a result of a tube rupture accident.

Environmental Consequences of a Tube Rupture

The occurrence of a steam generator tube rupture, followed by immediate loss of off-site electrical power, has an extremely low probability. The effects have, however, been analyzed and the results show that the public health and safety are not endangered.

The chronology of events subsequent to the tube failure is discussed above.

In assessing the consequences of the assumed accident, the inventory of halogens and noble gases available for release from the faulty steam generator is based on the following:

1. The activity concentration in the reactor coolant is assumed to arise from continuous operation with 1% defective fuel clad (see Table D.4-1).

2. It is assumed that the plant has been operating with a 5 gpm primary to secondary leak rate for a period of time sufficient to establish radionuclide equilibrium in the secondary loop without credit for blowdown treatment.

3. The corresponding iodine activity in the secondary system is the sum of the equilibrium value due to the pre-existing tube leak and that amount transferred with the reactor coolant due to the complete tube rupture. The value of this activity is 209 Ci of I^{131} equivalent, which is assumed at a uniform concentration in the secondary coolant.

4. The total noble gas inventory available for release is the sum of the equilibrium value due to the pre-existing tube leak, and that amount transferred with reactor coolant due to the complete tube rupture, and is equal to 21,700 Ci of Xe^{133} equivalent.

5. All releases are made by atmospheric steam dump from the faulty steam generator with an assumed decontamination factor of 10 applied to the iodines.

In calculating off-site plume centerline exposure, it is assumed that the activity is discharged to the atmosphere at ground level and is dispersed as a Gaussian plume downwind taking into account building wake dilution.

Dispersion coefficients based on the on-site meteorology program are used. A wind velocity of 1.5 meters per second is assumed to remain in one direction for the duration of the accident under Pasquill F conditions. The dispersion characteristics are discussed in Section 2.7.4 and curves corrected for building wake effects by the volumetric source method, are presented in Figure 2.7-8.

The following parameters have been used in the dose assessment:

- A 0-2 hour χ/Q value of 2.23E–4 sec/m^3
- Breathing rate equal to 3.47E–4 m^3/sec.
- An I^{131} equivalent dose conversion factor equal to 1.48E+6 rem/curie.
- A Xe^{133} dose conversion factor equal to 5.27E–2 rem–m^3/curie/sec.

Summary of Calculated Doses

The following tabulation summarizes the two-hour whole body and thyroid doses at the exclusion distance, consistent with a receptor on the plume centerline.

	Thyroid Dose	Whole Body Dose
Steam Generator Tube Rupture	2.4 rem	0.26 rem
10 CFR 100 Guidelines	300 rem	25 rem

It is concluded that the complete failure of a steam generator tube preceded by a long-term leak history prior to its failure would present no undue hazard to public health and safety.

In 1992, Westinghouse completed a study (Reference 3) addressing the radiological consequences of steam generator tube bundle uncovery coincident with a steam generator tube rupture, following a reactor trip. The results of the study indicated that there was little effect on radiological release due to tube uncovery, and that the 10 CFR 100 limits continued to be met. It was concluded that steam generator tube uncovery did not have significant impact on the accident analysis for steam generator tube rupture, and that no modifications to the analysis were necessary. A Westinghouse letter (Reference 4) transmitted the Westinghouse and NRC resolution stating that the issue was closed.

Recovery Procedure

The immediately apparent symptoms of a tube rupture accident such as falling pressurizer pressure and level, and increased charging pump flow are also symptoms of small steam-line breaks and loss of coolant accidents. It is therefore important for the operator to determine that the accident is a rupture of a steam generator tube to carry out the correct recovery procedure. The steam generator tube rupture is uniquely identified by high condenser air ejector radiation, high steam generator blowdown radiation, high steam line radiation, and decreased feedwater flow to the ruptured steam generator before the reactor trip. When the operators observe these indications, they enter the steam generator tube rupture recovery procedure.

The operators perform the following steps, which lead to isolation of the ruptured steam generator and termination of the leak.

1. Identify the ruptured steam generator by observing a higher level or higher radiation levels in one steam generator.

2. Isolate the ruptured steam generator by closing the main steam isolation valve and other smaller valves.

3. Stop auxiliary feedwater flow to the ruptured steam generator when the narrow range level returns to scale.

4. Control auxiliary feedwater flow in the intact steam generator so that the narrow range level remains on scale.

5. If off-site power is available, use condenser steam dumps to cool the Reactor Coolant System to enable RCS pressure to be reduced below the pressure of the ruptured steam generator. If

off-site power is not available, atmospheric steam dumps or steam generator power-operated relief valves are used.

6. If off-site power is available, depressurize the RCS to below the pressure of the ruptured steam generator using pressurizer spray valves. If off-site power is not available, the reactor coolant pumps would not be running, making spray unavailable. In this case pressurizer power-operated relief valves or auxiliary spray are used for the depressurization.

7. Stop safety injection pumps.

8. Cool the Reactor Coolant System to cold shutdown. The ruptured steam generator is depressurized by either backfill into the RCS, blowdown into the Steam Generator Blowdown Treatment System, or steam dump into the condenser or atmosphere.

After the Residual Heat Removal System (RHR) is in operation, the condensate accumulated in the secondary system can be sampled and processed.

There is ample time to carry out the above recovery procedure such that isolation of the ruptured steam generator is established before water level rises into the main steam lines. The available time scale is improved by the termination of auxiliary feedwater flow to the faulty steam generator. Normal operator vigilance therefore assures that excessive water level is not attained.

14.2.5 STEAM LINE BREAK

Accident Description

A steam-line break results in an uncontrolled steam release from a steam generator. The steam release results in an initial increase in steam flow, which decreases during the accident as the steam pressure falls. The energy removal from the Reactor Coolant System causes a reduction of coolant temperature and pressure. In the presence of a negative coolant temperature coefficient, the cooldown results in a reduction of core shutdown margin. If the most reactive RCCA is assumed stuck in its fully withdrawn position, there is an increased probability that the core becomes critical and returns to power. A return to power following a steam line break is a potential problem mainly because of the high hot channel factors that exist when the most reactive RCCA is assumed stuck in its fully withdrawn position. Assuming the most pessimistic combination of circumstances, which could lead to power generation following a steam-line break, the core is ultimately shut down by boric acid injection delivered by the Emergency Core Cooling System.

The analysis of a steam line break is performed to demonstrate that:

1. Assuming a stuck RCCA, with or without off-site power, and assuming a single failure in the engineered safety features, there is no consequential damage to the primary system and the core remains in place and intact.

2. Energy release to the containment from the worst steam line break does not cause failure of the containment structure.

3. There will be no return to criticality after reactor trip, for a break equivalent to the spurious opening, with failure to close, of the largest of any single steam dump, relief or safety valve.

The following systems and components provide the necessary protection against steam line breaks:

1. Safety Injection System actuation from any of the following:

   - Two-out-of-three low pressurizer pressure signals.
   - Two-out-of-three low-pressure signals in either steam line.
   - Two-out-of-three high containment pressure signals.

2. The overpower reactor trips (neutron flux and ΔT) and the reactor trip occurring in conjunction with receipt of the Safety Injection Signal.

3. Redundant isolation of the main feedwater lines: Sustained high feedwater flow would cause additional cooldown. Therefore, in addition to the normal control action, which closes the main feedwater valves, a safety injection signal rapidly closes all feedwater control valves, trips the main feedwater pumps, and closes the feedwater pump discharge valves.

4. Trip of the fast-acting main steam isolation valves (MSIVs). These valves are designed to close in less than five seconds on:

   - The coincidence of a Safety Injection Signal with either Hi-Hi steam flow from the respective steam line (one-out-of-two per line) or Hi steam flow from the respective steam line (one-out-of-two per line) in coincidence with Lo-Lo T_{avg} (two-out-of-four).
   - Two-out-of-three Hi containment pressure signals.

   Each steam line has a fast-closing MSIV with a downstream non-return check valve. These four valves prevent blowdown of more than one steam generator for any break location even if one valve fails to close. For example, in the case of a break upstream of the MSIV in one line, closure of either the non-return check valve in that line or the MSIV in the other line will prevent blowdown of the other steam generator. This arrangement precludes blowdown of more than one steam generator inside the containment and thus prevents structural damage to the containment.

5. Each main steam line incorporates a 16-inch diameter venturi type flow restrictor, which is located inside the containment. The venturi flow restrictors serve to limit the rate of release of steam.

Analysis - Core Response

The analysis of the steam line break has been performed to determine:

1. The core heat flux and Reactor Coolant System temperature and pressure resulting from the cooldown following the steam line break. A full plant digital computer simulation has been used.

2. The thermal and hydraulic behavior of the core following a steam line break. A detailed thermal and hydraulic digital computer calculation has been used to determine if DNB occurs for the core conditions computed in (1) above.

The following conditions are assumed to exist at the time of a steam-line break.

1. 2% end-of-life shutdown margin at no-load, equilibrium xenon conditions, with the most reactive rod stuck in its fully withdrawn position.

2. The negative moderator coefficient corresponding to the end-of-life rodded core with the most active rod in the fully withdrawn position. The variation of the coefficient with temperature and pressure has been included. The k vs. temperature relationship corresponding to the negative moderator temperature coefficient used is shown in Figure 14.2.5-1. In computing the power generation following a steam-line break, the local reactivity feedback from the high neutron flux in the region of the core near the stuck control rod has been included in the overall reactivity balance. The local reactivity feedback is composed of Doppler reactivity from the high fuel temperatures near the stuck control rod and moderator feedback from the high water enthalpy near the stuck rod. The effect of power generation in the core on total core reactivity is shown in Figure 14.2.5-2.

3. Minimum capability for injection of high concentration boric acid solution corresponding to the most restrictive single failure in the Safety Injection System. This corresponds to the flow delivered by one high-head Safety Injection Pump (see Figure 14.2.5-3). Boric acid concentration delivered to the reactor coolant loops corresponds to the minimum concentration of the Refueling Water Storage Tank, which is 2400 ppm.

4. To maximize the reactor cooldown steam generator tube plugging is at 0% and a conservatively high reactor coolant system flow is assumed.

5. Hot channel factors corresponding to the worst stuck rod at end-of-core life. The hot channel factors depend upon the core power, temperature, pressure, and flow.

6. Three break sizes have been considered in determining the core response transient.

   a) Complete severance of a pipe downstream of the steam flow restrictor, with the plant initially at no-load conditions and both reactor coolant pumps running.

b) Complete severance of a pipe inside the containment at the outlet of the steam generator, with the plant at no-load conditions and both reactor coolant pumps running.

c) A break equivalent to the inadvertent opening of one steam generator safety valve at 1100 psi from one steam generator with off-site power available.

d) Initial hot shutdown conditions were considered for all of the above cases since this represents the most pessimistic initial condition for the accident. Should the reactor be critical at the time of a steam-line break, it would be tripped by the normal overpower protection system when power level reaches a trip point. Following a trip at power, the reactor coolant system contains more stored energy than at no-load, the average coolant temperature is higher than at no-load, and there is appreciable energy stored in the fuel. Thus, the additional stored energy is removed via the cooldown caused by the steam-line break before the no-load conditions of reactor coolant system temperature and shutdown margin assumed in the analyses are reached. After the additional stored energy has been removed, the cooldown and reactivity insertions proceed in the same manner as in the analysis that assumes no-load condition at time zero. However, since the initial steam generator water inventory is greatest at no-load, the magnitude and duration of the Reactor Coolant System cooldown are less for steam breaks occurring at power.

7. Steam break flow is given by $W_{Break} = A_{Break}(t) * G(h_1P)$ where $A_{Break}(t)$ is the specified break area as a function of time and $G(h_1P)$ is the mass flow rate per unit area. G is evaluated from Moody flow tables (Reference 5). For a break in the steam line perfect moisture separation in the steam generator is assumed.

Results

The results presented are a conservative indication of the events that would occur assuming a main steam-line break, since it is postulated that all of the conditions described above occur simultaneously.

Figures 14.2.5-9 through 14.2.5-13 show the results following a main steam-line break (complete severance of the pipe) downstream of the flow restrictor, at initial no-load conditions and with outside power available. Core heat flux increases and is stabilized by the negative reactivity feedbacks from rising fuel temperatures and increased enthalpy in the region of the stuck rod.

The analysis assumes the boric acid of the Safety Injection System is mixed with, and diluted by, the water flowing in the RCS prior to entering the reactor core. The concentration after mixing depends upon the relative flow rates in the Reactor Coolant System and in the Safety Injection System. The variation of mass flow rate in the RCS due to water density changes is included in the calculation, as is the variation of flow rate in the Safety Injection System due to changes in the Reactor Coolant System pressure. The Safety Injection System flow calculation includes the line losses in the system as well as the pump head curve.

Figures 14.2.5-4 through 14.2.5-8 show results from a main steam-line break at the exit of a steam generator upstream of the flow restrictor. The sequence of events is similar to those

described above except that criticality is attained earlier due to the more rapid cooldown, and a higher maximum core average heat flux is attained.

Figures 14.2.5-14 through 14.2.5-18 show the transient resulting from a break equivalent to an inadvertent opening of one S/G safety valve at 1100 psi from one steam generator.

A DNBR analysis is performed for the break upstream and break downstream of the flow restrictor cases. DNBR is calculated for the core conditions that existed at the time of maximum core heat flux. A conservatively high value for hot channel factor (F_H) is also assumed. The following table shows the comparison of the important calculated safety parameters to its acceptance criteria (Calculated Value/Acceptance Criterion):

MSLB	MDNBR
Downstream of flow restrictor	3.106/1.45
Upstream of flow restrictor	1.603/1.45

Analysis - Containment Response

There are four major factors that influence the release of mass and energy following a steam-line break. These are the initial steam generator fluid inventory, primary to secondary heat transfer, protective system operation, and the state of the secondary fluid blowdown. The following is a list of those plant variables that determine the influence of each of these factors.

- Plant Power Level
- Main Feedwater System Design
- Auxiliary Feedwater System Design
- Break Type, Area, Location
- Availability of Offsite Power
- Steam Generator Design
- Safety System Failures
- SG Reverse Heat Transfer and Reactor Coolant System Metal Heat Capacity

All of these variables are considered in the analyses and are conservatively selected based on Kewaunee plant design.

Steam-line break analysis cases are described based on a specific set of five parameters in the following manner:

1. Power Level: 0, 30, 70, and 102% for the rated power level.

2. Break Size and Location:

   a) Location is either upstream or downstream of the steam line flow restrictor. Upstream case is a large (4.29 ft^2) break; downstream cases are 1.4 ft^2 or less.

b) Break size:

- Break downstream of the flow restrictor with an effective area equal to that of the flow restrictor (A_{FR} = 1.4 ft^2).
- Break downstream of the flow restrictor with an area less than A_{FR}. (Break areas ranging from 1.4 ft^2 to 0.1 ft^2 are considered)
- Split break with maximum area for which MSIV isolation results from a containment signal and entrainment does not occur.
- Break upstream from the flow restrictor with an area equal to the SG outlet nozzle (4.29 ft^2) for the broken SG and equal to A_{FR} for the other SG.

3. Single Failures: There are three single failures which are:

- One Feedwater (FW) Regulating Valve fails to isolate. This is denoted as R.
- One Main Steam Isolation Valve (MSIV) fails to isolate. This is denoted as M.
- One Containment Safeguards Train (one containment safeguard train is: one internal containment spray train and two containment fan cooler units) fails to activate. This is denoted as N.

4. Off-Site Power: Cases with and without the availability of off-site power are considered.

5. Entrainment: The quality of steam exiting the break is explicitly modeled and is dependent on break size and power level.

Based on the above parameters, steam-line break analysis cases are designated as follows:

- Break Size (Units of ft^2)
- Single Failure
  R - FW Reg Valve Failure
  M - MSIV Failure
  N - Containment Safeguards System Failure
- Off-Site Power
  Y - Yes
  N - No
- Entrainment
  Y - Yes
- Power Level
  0 - 0%
  3 - 30%
  7 - 70%
  2 - 102%

For identification purposes, the cases are represented by a six number/letter identification tag. For example:

14NYY3 represents the steam line break case with:

14	=	1.4 ft^2 break
N	=	single active failure is one containment safeguards train
Y	=	off-site power is available
Y	=	entrainment is modeled
3	=	initial power level is 30%

Further descriptions of the methods for steam-line break analysis follow:

1. The main feedwater flow is calculated using the following assumptions:

   a) The feedwater pumps are running at full speed at the start of the transient and are tripped off on the safety injection signal. A conservative flow coastdown is modeled.

   b) The condensate pumps are running at full speed throughout the transient.

   c) The regulating valve for the unfaulted Loop remains at its initial position until the time at which it strokes to its fully-closed position at a rate of 5%/sec following an isolation signal. At that time, the valve is closed instantaneously.

   d) The behavior of the regulating valve for the faulted loop is assumed to begin opening at t = 0.0 sec at an 8%/sec rate until the time the isolation signal occurs. It is held at that position until the time at which it strokes to its fully-closed position at a rate of 5%/sec following the isolation signal. At that time, the valve is closed instantaneously. For cases with a regulating valve failure, isolation is produced by closure of the FW isolation valve. The assumption used for the isolation valve is that it begins to close, at the time of the isolation signal, from full-open at a rate of 1.18%/sec. The initial opening of the regulating valve and the instantaneous FW isolation valve closure at the end of the stroke time are the same as for the case without a regulating valve closure failure.

2. The auxiliary feedwater (AFW) flow split between the two SGs is modeled the AFW is initiated, prior to the time for the activation signal, at full capacity and using a conservatively high enthalpy. All three AFW pumps are assumed to be operating.

3. The core physics parameters are based on a bounding set corresponding to end-of-cycle conditions and minimum technical specification shutdown requirements. The scram worth includes having the most reactive rod stuck out.

4. The dynamic reactor coolant pump model is used, which includes the gravity head and pump heat effects.

5. Conservative setpoints and time delays are used throughout.

6. No credit is taken for charging flow.

7. No credit is taken for SG tube plugging.

8. The following considerations are made in modeling the steam lines.

   a) The pressure balancing line is modeled to allow communication between the steam lines in an unrestricted manner.

   b) Main steam isolation for the unfaulted loop is assumed to occur instantaneously at the time required for the non-return check valve to close in the faulted loop, which is 5 seconds after the break occurs.

   c) MSIV failure is modeled as a failure of the non-return check valve in the faulted loop. Steam flow from the unfaulted loop continues until the MSIV in the unfaulted main steam line closes. A closure assumption of 5 seconds is used for the MSIV. The time from the event initiation until MSIV closure signal receipt, plus signal instrumentation delays as applicable to the accident sequence analyzed, is added to the 5 second MSIV closure time assumption. At the time of the MSIV closure, the entire faulted and unfaulted loop steam lines from the MSIV to the turbine and the pressure balancing line are added to the total fluid mass and energy input to containment.

9. Entrainment analysis methods are used to obtain the time dependent quality of the faulted steam-line break flow which is power level and break size dependent. The quality of the unfaulted steam line break flow is conservatively assumed to be 1.0.

10. The turbine is tripped at t = 0.0 seconds for 0% power cases, and prior to or at the actual time of reactor trip for at power cases. These are conservative assumptions that maximize the available steam for blowdown.

11. A constant containment back pressure of 14.7 psia is conservatively assumed in all cases.

12. A conservatively high RCS flow rate is assumed.

13. Steam generator fluid inventory is maximized. Initial steam generator water level is 50.0% nominal narrow range level for all cases.

Results

Figures 14.2.5-19 and 14.2.5-20 present containment pressure and temperature responses for the limiting containment response steam line break analysis cases. The table below shows, for these limiting cases, the peak calculated containment pressure, temperature and the corresponding acceptance criteria. All cases analyzed result in a maximum containment pressure that is less than the containment design pressure limit of 60.7 psia. In addition, the limiting containment temperature profile has been evaluated and it does not create an equipment qualification

concern. Although the limiting temperature profile exceeds the containment design temperature of 268°F, containment structural limits are not exceeded. The short duration of the temperature spike and the method of heat transfer to the containment shell precludes shell temperature from exceeding the design temperature.

MSLB	Containment Peak Pressure (psia)	Containment Peak Temperature (°F)
14MYY0	60.5/60.7	267.7/330.0
01NYY0	39.3/60.7	298.9/330.0

Conclusions

The analyses have shown that the main steam line break acceptance criteria are satisfied.

Although DNB and possible clad perforation are not precluded in the acceptance criteria, the safety analysis has demonstrated that DNB does not occur, provided that core $F_{\Delta H}$ under steam line break conditions is ≤ 5.00.

The peak pressure for the limiting containment response cases can not exceed the containment design pressure. The limiting temperature profile also does not create an environmental qualification concern for equipment in containment.

Based on the preceding analyses, the radiological significance of a steam line break would depend on the activity levels in the secondary loop of the failed steam generator. The consequence of a long-term 5-gpm leak rate has been considered in Section 14.2.4. However, even if it is conservatively assumed that all of the reactor coolant activity associated with 1% defective fuel cladding is suddenly expelled into the steam generator the resultant thyroid dose at the site boundary would be 4.7 rem and the resultant whole body dose would be 0.51 rem. A decontamination factor of 10 has been applied to the iodine inventory. The consequences of these postulated accidents are well below the guidelines of 10 CFR 100.

14.2.6 RUPTURE OF A CONTROL ROD DRIVE MECHANISM HOUSING (RCCA EJECTION)

Description of Accident

This accident is a result of an extremely unlikely mechanical failure of a control rod mechanism pressure housing such that the Reactor Coolant System pressure would then eject the RCCA and drive shaft. The consequences of this mechanical failure, in addition to being a minor loss-of-coolant accident, may also be a rapid reactivity insertion together with an adverse core power distribution, possibly leading to localized fuel rod damage for severe cases. The resultant core thermal power excursion is limited by the Doppler reactivity effect of the increased fuel temperature and terminated by reactor trip actuated by high neutron flux signals.

Certain features in Westinghouse pressurized water reactors are designed to preclude the possibility of a rod ejection accident, and to limit the consequences if the accident were to

occur. These include a sound conservative mechanical design of the rod housings, together with a thorough quality control (testing) program during assembly, and a nuclear design which lessens the potential ejection worth of RCCAs and minimizes the number of assemblies inserted at high power levels.

The mechanical design is discussed in Section 3. An evaluation of the mechanical design and quality control procedures indicates that a failure of a control rod mechanism housing sufficient to allow a control rod to be rapidly ejected from the core should not be considered credible for the following reasons:

1. Each control rod drive mechanism housing is completely assembled and shop-tested at 4100-psi.

2. The mechanism housings are individually hydro-tested as they are installed to the head adapters in the reactor vessel head, and checked during the hydro-test of the completed Reactor Coolant System.

3. Stress levels in the mechanism are not affected by anticipated system transients at power, or by the thermal movement of the coolant loops. Movements induced by the design earthquake can be accepted within the allowable primary working stress range specified by the ASME Code, Section III, for Class A components.

4. The latch mechanism housing and rod travel housing are each a single length of forged Type-304 stainless steel. This material exhibits excellent notch toughness at all temperatures that are encountered.

A significant margin of strength in the elastic range, together with the large energy absorption capability in the plastic range, gives additional assurance that gross failure of the housing will not occur. The joints between the latch mechanism housing and head adapter, and between the latch mechanism housing and rod travel housing, are threaded joints reinforced by canopy-type rod welds. Administrative regulations require periodic inspections of these (and other) welds.

Even if a rupture of the control rod mechanism housing is postulated, the operation of a chemical shim plant is such that the severity of an ejected RCCA is inherently limited. In general, the reactor is operated with RCCAs inserted only far enough to permit load follow. Reactivity changes caused by core depletion and xenon transients are compensated by boron changes. Further, the location and grouping of control rod banks are selected during nuclear design to lessen the severity of an ejected assembly. Therefore, should an RCCA be ejected from the reactor vessel during normal operation, there would probably be no reactivity excursion since most of the RCCAs are fully withdrawn from the core, or a minor reactivity excursion if an inserted assembly is ejected from its normal position.

However, it may occasionally be desirable to operate with larger than normal insertions. For this reason, a rod insertion limit is defined as a function of power level. Operation with the RCCAs above this limit guarantees adequate shutdown capability and acceptable power distribution. The position of all assemblies is continuously indicated in the control room. An alarm will occur if

a bank of RCCAs approaches its insertion limit or if one assembly deviates from its bank. There are low and low-low level insertion monitors with visual and audio signals. Operating instructions require normal boration when receiving either alarm. The RCCA position monitoring and alarm systems are described in detail in Section 7.

The reactor protection in the event of a rod ejection accident has been described in WCAP-7306 (Reference 6).

Disregarding the remote possibility of the occurrence of a control rod mechanism housing failure, investigations have shown that failure of a control rod housing due to either longitudinal or circumferential cracking does not cause damage to adjacent housings such that the severity of the initial accident increases.

Due to the extremely low probability of a rod ejection accident, some fuel damage could be considered an acceptable consequence, provided there is no possibility of the off-site consequences exceeding the guidelines of 10 CFR 100. Although severe fuel damage to a portion of the core may in fact be acceptable, it is difficult to treat this type of accident on a sound theoretical basis. For this reason, criteria for the threshold of fuel failure are established, and it is demonstrated that this limit is not exceeded.

Comprehensive studies of the threshold of fuel failure and of the threshold of significant conversion of the fuel thermal energy to mechanical energy have been carried out as part of the SPERT project by the Idaho Nuclear Corporation (Reference 7). Extensive tests of UO_2 - Zirconium-clad fuel rods representative of those in PWR-type cores have demonstrated failure thresholds in the range of 240 to 257 cal/g. However, other rods of a slightly different design have exhibited failures as low as 225 cal/g. These results differ significantly from the TREAT (Reference 8) results, which indicated a failure threshold of 280 cal/g. Limited results have indicated that this threshold decreases by about 10% with fuel burnup. The clad failure mechanism appears to be melting for zero burnup rods and brittle fracture for irradiated rods. Also important is the conversion ratio of thermal to mechanical energy. This ratio becomes marginally detectable above 300 cal/g for unirradiated rods and 200 cal/g for irradiated rods; catastrophic failure, (large fuel dispersal, large pressure rise) even for irradiated rods, does not occur below 300 cal/g.

In view of the above experimental results, criteria are applied to ensure that there is little or no possibility of fuel dispersal in the coolant, gross lattice distortion, or severe shock waves. These criteria are:

a. Average fuel pellet enthalpy at the hot spot below 200 cal/g.

b. Average clad temperature at the hot spot below the temperature at which clad embrittlement may be expected.

c. Peak reactor coolant pressure much less than that which would cause damage to the Reactor Coolant System.

The temperature at which clad embrittlement may become a problem for this accident is presently taken to be 2700°F. The peak reactor coolant pressure limit is taken to be 2750 psia, which is much less than that required to strain the reactor vessel or piping to its minimum specified yield strength.

Method of Analysis

The analysis of the control rod ejection accident requires modeling of the neutron kinetics coupled with the fuel and clad heat up condition and the thermal hydraulics of the coolant channel. The analysis is performed by first calculating the core average neutronic response and then using the resulting core average power response as a forcing function for the hot spot thermal evaluation.

A 1-D axial kinetics model is used for the analysis of the core average response since it allows for a more realistic representation of the spatial effects of axial moderator feedback, power distribution, and RCCA movement. The moderator reactivity effect is included by correlating reactivity with moderator density, thereby including the effects of coolant temperature, pressure, and voiding. The Doppler reactivity effect is correlated as a function of fuel temperature. The largest temperature rise during the transient, and hence the largest reactivity effects, occurs in channels where the power is higher than average. As a result, when a 3-D space time kinetics calculation is not performed, weighting factors are applied as multipliers to the average channel Doppler reactivity feedback to account for spatial reactivity feedback effects.

The average core energy addition, calculated as described above, is multiplied by the appropriate hot channel factors, and the hot spot analysis is performed using a detailed fuel and clad transient heat-transfer computer code. This computer code calculates the transient temperature distribution in a cross-section of a metal-clad UO_2 fuel rod, and the heat flux at the surface of the rod, using as input the nuclear power vs time and the local coolant conditions. The zirconium-water reaction is explicitly represented, and all material properties are represented as functions of temperature. A parabolic radial power distribution is used within the fuel rod.

The computer code uses the Dittus-Boelter or Jens-Lottes correlation to determine the film heat transfer before DNB, and a transition boiling correlation to determine the film boiling coefficient after DNB (Reference 9). The DNB heat flux is not calculated, instead the code is forced into DNB by specifying a conservative DNB heat flux. The gap heat-transfer coefficient may be calculated by the code; however, it is adjusted in order to force the full-power steady-state temperature distribution to agree with the fuel heat-transfer design codes.

Since the calculations result in maximum fuel enthalpies less than those corresponding to catastrophic fuel failures, the system pressure surge is calculated on the basis of conventional heat transfer from the fuel. The pressure surge model includes prompt heat generation in the coolant, fluid transport in the system, heat transfer in the steam generators, and the action of relief and safety valves. No credit is taken for pressure reduction caused by the assumed failure of the control rod pressure housing.

Input parameters for the analysis are conservatively selected on the basis of values calculated for this core. The more important parameters are discussed below. The following table presents the parameters used in this analysis:

Initial Core Conditions	BOC HZP	BOC HFP	EOC HZP	EOC HFP
Ejected Rod Worth, % ΔK	0.91	0.30	0.92	0.42
Delayed Neutron Fraction, %	0.70	0.70	0.50	0.50
Feedback Reactivity Weighting	1.30	1.60	1.30	1.60
F_q After Rod Ejection	8.20	5.03	11.7	5.10
Number of Pumps Operational	1	2	1	2

The values for ejected rod worths and hot channel factors are calculated using a synthesis of one dimensional and two dimensional calculations. No credit is taken for the flux flattening effects of reactivity feedback. The calculation is performed for the maximum allowed bank insertion at a given power level, as determined by the Rod Insertion Limits. An uncertainty of 10% is conservatively added to the ejected rod worth.

The total transient hot channel factor, F_q, is then obtained by multiplying the axial and radial factors, even though the axial peaks are not coincident under the conditions of calculation.

Moderator reactivity assumptions are conservative compared to actual design values. Positive moderator temperature coefficient is assumed for the beginning of cycle zero power case. No weighting factor is applied to the moderator reactivity feedback. The Doppler reactivity is determined as a function of power level and fuel temperature and is conservative when compared to actual design values. A Doppler weighting factor is applied to account for the missing dimensions in the 1-D axial kinetics simulation. This weighting factor is conservative when compared to 3-D space time kinetics computation.

Calculations of the effective delayed neutron fraction (βeff) have yielded values of no more than 0.70% at BOC and no < 0.50% at EOC. The accident is sensitive to β if the ejected rod worth is equal to or greater than β, as in the zero power transients. No uncertainty is applied directly to the value of β since the calculation of the rod worth and hot channel factor is considered very conservative.

The trip reactivity used for the analysis is shown in Figure 14.0.1. The start of rod motion occurs 0.5 seconds after the high neutron flux trip point is reached. This delay is assumed to consist of 0.2 seconds for the instrument channel to produce a signal, 0.15 seconds for the trip breaker to open and 0.15 seconds for the coil to release the rods. The choice of such a conservative insertion rate and delay for rod motion means that there is over 1 second after the trip setpoint is reached before significant shutdown reactivity is inserted into the core. This is a particularly important conservatism for hot full-power accidents.

Results

Results of the beginning and end of life full and zero power rod ejection analyses are shown in Figures 14.2.6-1 through 14.2.6-12. These results are also summarized below. The acceptance

criteria on average fuel enthalpy (200 cal/g) and average clad temperature (2700°F) are not exceeded. Therefore, fuel is not expected to be dispersed into the coolant under the most severe conditions of this transient.

It is assumed that fission products are released from the gaps of all rods entering DNB. In all cases considered, < 15% of the rods entered DNB. (This corresponds to 2% of the core volume.) The position with regard to fission product release is therefore much better than the double-ended coolant pipe break.

A detailed calculation of the pressure surge shows that assuming an initial pressure of 2250 psia, the peak pressure reached in the transient is well within the criteria of 2750 psia and, therefore, no damage to the Reactor Coolant System will occur.

In the region of the hot spot there is a large temperature gradient. Since the fuel rods are free to move in the vertical direction, differential expansion between separate rods cannot produce distortion. However, the temperature gradients across individual rods may produce a force tending to bow the mid-point of the rods toward the hot spot. Physics calculations indicate that the net result of this is a negative reactivity insertion. In practice, no significant bowing is anticipated since the structural rigidity of the core is more than sufficient to withstand the forces produced.

Boiling in the hot spot region produces a net flow away from that region. However, the fuel heat is released to the water relatively slowly, and it is considered inconceivable that cross flow would be sufficient to produce significant lattice forces. Even if massive and rapid boiling, sufficient to distort the lattice, is hypothetically postulated, the large void fraction in the hot spot region would produce a reduction in the total core moderator to fuel ratio, and a large reduction in this ratio at the hot spot. The net effect would therefore be a negative feedback. It is concluded that no conceivable mechanism exists for a net positive feedback resulting from lattice deformation. In fact, a small negative feedback would result. The effect was conservatively ignored in the analyses.

The following table shows a comparison of the important calculated safety parameters to their respective acceptance criteria (Calculate Value/Acceptance Criterion):

Control Rod Ejection	Max Clad Temp. (°F)	Max Fuel Centerline Temp.(°F)	Max Energy Deposition (cal/g)	RCS Pressure (psia)	MS Pressure (psia)
BOC Full Power	2133/2700	4682/4700	157/200	2285/2750	863/1210
BOC Zero Power	2555/2700	3925/4700	174/200	2306/2750	1027/1210
EOC Full Power	2091/2700	4652/4700	154/200	2297/2750	864/1210
EOC Zero Power	2688/2700	4031/4700	182/200	2277/2750	1022/1210

Conclusions

Even on the most pessimistic basis, the analyses indicated that the fuel and clad limits were not exceeded. It was concluded that there was no danger of sudden fuel dispersal into the

coolant. The pressure surge was shown to be insufficient to exceed 2750 psia, and it was concluded that there was no danger of consequential damage to the primary coolant system. The amount of fission products released as a result of clad rupture during DNB is considerably less than in the case of the double-ended main coolant pipe break (the Design Basis Accident), and therefore within the guidelines of 10 CFR 100.

14.2.7 TURBINE MISSILE DAMAGE TO SPENT FUEL POOL

DELETED

REFERENCES - SECTION 14.2

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2. NRC Safety Evaluation Report, "Kewaunee Nuclear Power Plant, Supplemental Safety Evaluation Associated with Amendment No. 132", WO Long (NRC) to ML Marchi (WPSC), Letter No. K-98-124 dated September 3, 1998

3. WCAP-13132, "The Effect of Steam Generator Tube Bundle Uncovery on Radioiodine Release" January 1992 (proprietary)

4. Letter WOG 98-066 from KJ Voytell (WOG) to primary representatives (WPS), Transmittal of NRC Closure Letter on the Steam Generator Tube Rupture Uncovery Issue dated March 31, 1993

5. FJ Moody, "Maximum Flow Rate of a Single Component, Two-Phase Mixture" transactions of the American Society of Mechanical Engineers, February 1965, page 136.

6. TWT Burnett, "Reactor Protection System Diversity in Westinghouse Pressurized Water Reactor" WCAP-7306, April 1969

7. TG Taxelius, ed. "Annual Report - Spert Project, October 1968, September 1969" Idaho Nuclear Corporation TID-4500, June 1970

8. RC Liimatainen and FJ Testa, "Studies in TREAT of Zircaloy-2-Clad, UO_2 - Core Simulated Fuel Elements" Argonne National Laboratory Chemical Engineering Division Semi-Annual Report, ANL-7225, January-June 1966

9. RF Farman and JO Cermak, "Post DNB Heat Transfer During Blowdown" WCAP-7837, January 1972

14.3 REACTOR COOLANT SYSTEM PIPE RUPTURES (LOSS OF COOLANT ACCIDENT)

General

Condition III - Infrequent Faults

By definition, Condition III occurrences are faults which may happen very infrequently during the life of the plant. They will be accommodated with the failure of only a small fraction of the fuel rods although sufficient fuel damage might occur to preclude resumption of the operation for a considerable outage time. The release of radioactivity will not be sufficient to interrupt or restrict public use of those areas beyond the exclusion radius. A Condition III fault will not, by itself, generate a Condition IV fault or result in a consequential loss of function of the Reactor Coolant System or of containment barriers.

The time sequence of events for the small breaks is shown in Table 14.3-1, and Table 14.3-1a presents the results of the analysis.

Condition IV - Limiting Faults

Condition IV occurrences are faults which are not expected to take place, but are postulated because their consequences would include the potential for the release of significant amounts of radioactive material. These are the most drastic occurrences which must be designed against, and they represent limiting design cases. Condition IV faults are not to cause a fission product release to the environment resulting in an undue risk to public health and safety in excess of guideline values of 10 CFR 100. A single Condition IV fault is not to cause a consequential loss of required functions of systems needed to cope with the fault including those of the Emergency Core Cooling System and the containment.

The analysis of thyroid and whole body doses, resulting from events leading to fission product release, appears in Section 14.3.5. The fission product inventories, which form a basis for these calculations are presented in Appendix D. Sections 14.3.4, 14.3.5 and Appendix H also include the discussions of systems interdependency contributing to limiting fission product leakage from the containment following a Condition IV occurrence.

The time sequence of events for a large break is shown in Table 14.3.2-8 and Figure 14.3.2-1, and Table 14.3.2-9 presents the results of these analyses.

14.3.1 LOSS OF REACTOR COOLANT FROM SMALL RUPTURED PIPES OR FROM CRACKS IN LARGE PIPES WHICH ACTUATES EMERGENCY CORE COOLING SYSTEM

Identification of Causes and Accident Description

A loss-of-coolant accident is defined as a rupture of the Reactor Coolant System piping or of any line connected to the system. See Section 4.1.3 for a more detailed description of the

loss of reactor coolant accident boundary limits. Ruptures of small cross sections will cause expulsion of the coolant at a rate which can be accommodated by the charging pumps which would maintain an operational water level in the pressurizer permitting the operator to execute an orderly shutdown. The coolant, which would be released to the containment, contains the fission products existing in it.

Should a larger break occur, depressurization of the Reactor Coolant System causes fluid to flow to the Reactor Coolant System from the pressurizer resulting in a pressure and level decrease in the pressurizer. Reactor trip occurs when the pressurizer low-pressure trip setpoint is reached. The Safety Injection System is actuated when the appropriate setpoint is reached. The consequences of the accident are limited in two ways:

1) Reactor trip and borated water injection complement void formation in causing rapid reduction of nuclear power to a residual level corresponding to the delayed fission and fission product decay,

2) Injection of borated water ensures sufficient flooding of the core to prevent excessive clad temperature.

Before the break occurs the plant is in an equilibrium condition, i.e., the heat generated in the core is being removed via the secondary system. During blowdown, heat from decay, hot internals and the vessel continues to be transferred to the Reactor Coolant System. The heat transfer between the Reactor Coolant System and the secondary system may be in either direction depending on the relative temperatures. In the case of continued heat addition to the secondary side, system pressure increases and steam dump may occur. Makeup to the secondary side is provided by the auxiliary feedwater pumps. The safety injection signal stops normal feedwater flow by closing the main feedwater line isolation valves and initiates emergency feedwater flow by starting auxiliary feedwater pumps. As discussed in Section 6.6, manual initiation of auxiliary feedwater is acceptable at low power levels. The secondary flow aids in the reduction of Reactor Coolant System pressure. When the RCS depressurizes to 700 psig, the accumulators begin to inject water into the reactor coolant loops. Reactor coolant pump trip is assumed to be coincident with the reactor trip and effects of pump coast-down are included in the blowdown analyses.

Analysis of Effects and Consequences

Method of Analysis

The requirements of an acceptable ECCS Evaluation Model are presented in Appendix K of 10 CFR 50 (Reference 1). The requirements of Appendix K regarding specific model features were met by selecting models, which provide a significant overall conservatism in the analysis. The assumptions made pertain to the conditions of the reactor and associated safety system equipment at the time that the LOCA occurs and include such items as the core peaking factors, the containment pressure, and the performance of the ECCS system. Decay heat generated throughout the transient is also conservatively calculated as required by

Appendix K of 10 CFR 50. The small break Loss-of-Coolant Accident analysis is documented in WCAP-14103 dated June 1994 (Reference 6).

Small Break LOCA Analysis Using NOTRUMP

The Westinghouse NOTRUMP Small Break Evaluation Model consists of the NOTRUMP and SBLOCTA computer codes. NOTRUMP is used to model the system hydraulics and SBLOCTA calculates the fuel rod cladding heatup.

The postulated small break LOCA is predominately a gravity dominated accident in which the slow draining of the RCS is accompanied by the formation of distinct mixture levels throughout the RCS. These mixture levels vary with time and are dependent upon the transient two-phase transport of mass and energy, which takes place within the RCS during the course of the accident. Consequently, the degree of accuracy with which a system model is capable of simulating the RCS response to a small break LOCA is dependent upon the model's capability to accurately model the RCS transient mass and energy distribution.

For postulated LOCAs due to small breaks the NOTRUMP computer code is used to calculate the transient depressurization of the RCS as well as to describe the mass and enthalpy of flow through the break. The NOTRUMP computer code is a state-of-the-art one-dimensional general network code incorporating a number of advanced features. Among these are calculation of thermal non-equilibrium in all fluid volumes, flow regime-dependent drift flux calculations with counter-current flooding limitations, mixture level tracking logic in multiple-stacked fluid nodes and regime dependent heat transfer correlation. The Westinghouse NOTRUMP Small Break Evaluation Model was developed to determine the RCS response to design basis small break LOCAs, and to address NRC concerns expressed in NUREG-0611, "Generic Evaluation of Feedwater Transients and Small Break Loss-of-Coolant Accident in Westinghouse-Designed Operating Plants".

NOTRUMP (Reference 2 and 3) is a general one-dimensional nodal network computer code which describes the spatial detail of the RCS with a network of fluid nodes (representing various system fluid volumes), flow links (representing various fluid flow paths), metal nodes (representing various metal masses), and heat transfer links (representing various heat transfer paths between metal structures and surrounding fluid). The use of NOTRUMP in the analysis involves, among other things, the representation of the reactor core as heated control volumes with an associated phase separation model to permit a transient mixture height calculation. The broken loop and intact loop are each modeled explicitly. Transient behavior of the system is determined from the governing conservation equations of mass, energy and momentum. The multi-node capability of the program enables explicit, detailed spatial representation of various system components which, among other capabilities, enables a proper calculation of behavior of loop seal during a postulated small break LOCA.

Peak clad temperature calculations are performed with the SBLOCTA code (Reference 4), using the NOTRUMP calculated core pressure, fuel rod power history, uncovered core steam flow and mixture heights as boundary conditions. The code evaluates the fuel cladding and the coolant temperatures during the hypothetical small break LOCA. Each of the fuel rods

modeled by SBLOCTA is analyzed using finite-difference conduction equations in both the axial and radial directions. It calculates the effect of cladding swell and burst and considers the exothermic reaction between zircaloy and water. The Kewaunee hot rod axial power shape used to perform the small break analysis is described in reference 6. This type of shape is chosen because the power is concentrated in the upper regions of the core. Such a distribution is limiting for small-break LOCAs because it minimizes coolant swell, while maximizing vapor superheating and fuel rod heat generation at the uncovered elevations. The small break LOCA analysis assumes the core continues to operate at full rated power until the control rods are completely inserted.

Small break LOCA calculations are based on minimum safeguards assumptions designed to minimize pumped ECCS flow to the core. These calculations include loss of a train of ECCS, high head SI pump degradation of pump head by 10%, and the minimum ECCS line is modeled to spill (thus producing minimum delivered flow for the given pressure). Also, for a small break LOCA, pumped ECCS is assumed to spill to containment pressure if the equivalent diameter of the break is greater than or equal to the inner diameter of the SI line (see Figure 14.3-1a). This is assumed since the modeled break may include the severance of the SI line or an area of a severed SI line. Therefore, the pumped ECCS is conservatively assumed to spill to the containment pressure, which reduces the amount of SI flow, which can be delivered to the core. For breaks which have an equivalent diameter less than the safety injection line inner diameter, safety injection flow is assumed to spill to the RCS back-pressure (see Figure 14.3-1b). The effect of flow from the RHR pumps is not considered here since their shutoff head is lower than RCS pressure during the time portion of the transients considered here. Thus, the UPI design is not a factor in this analysis.

Delivery of the SI flow to the RCS was assumed to be delayed 30 seconds after the generation of a SI signal. This delay includes the time required for diesel startup and loading of the SI pumps onto the emergency buses and for the pump to come to full speed in order to deliver full flow. The assumed delay time is sufficient to account for degraded grid conditions.

Results

Reactor Coolant System Pipe Breaks

This section, presents results of the limiting break size in terms of highest peak clad temperature. The worst break size (small break) is a 3-inch diameter break. The depressurization transient for this break is shown in Figure 14.3-lc. The extent to which the core is uncovered is shown in Figure 14.3-2a.

During the earlier part of the small break transient, the effect of the break flow is not strong enough to overcome the flow maintained by the reactor coolant pumps through the core as they are coasting down following reactor trip. Therefore, upward flow through the core is maintained. The resultant heat transfer cools the fuel rod and clad to very near the coolant temperatures as long as the core remains covered by a two-phase mixture.

The maximum hot spot clad temperature calculated during the transient is shown in Table 14.3-1a, and includes the effects of fuel densification as described in Reference 5. The peak clad temperature transients are shown in Figure 14.3-2b for the worst break size, i.e., the break with the highest peak clad temperature. The steam flow rate for the worst break is shown on Figure 14.3-3a. When the mixture level drops below the top of the core, the steam flow computed by NOTRUMP provides cooling to the upper portion of the core. The core heat transfer coefficients for this phase of the transient are given in Figure 14.3-3b. The hot spot fluid temperature for the worst break is shown in Figure 14.3-4a.

The core power (dimensionless) transient following the accident (relative to reactor shutdown time) is shown in Figure 14.3-4b.

The reactor shutdown time (5.0-sec) is equal to the reactor trip signal time (2.0 sec) plus 3.0 sec for rod insertion. During this rod insertion period, the reactor is conservatively assumed to operate at rated power.

Conclusions

Analyses presented in this section show that the high head portion of the Emergency Core Cooling System, together with accumulators, provide sufficient core flooding to keep the calculated peak clad temperatures below required limits of 10 CFR 50.46. Hence, adequate protection is afforded by the Emergency Core Cooling System in the event of a small break loss-of-coolant accident.

Following the TMI accident, Westinghouse performed generic studies of small break loss-of-coolant accidents. Results of these studies indicated that peak clad temperatures greater than 2200°F may occur if the reactor coolant pumps are tripped after a significant loss of reactor coolant inventory. To prevent such a loss, the operators are instructed to trip the pumps early in the accident.

Additional Break Sizes

Additional break sizes were analyzed. Figures 14.3-5a and 14.3-5b present the RCS pressure transient for the 2- and 4-inch breaks, respectively and Figures 14.3-6a and 6b present the volume history (mixture height) plots for both breaks. The peak clad temperatures for both cases are less than the peak-clad temperature of the 3-inch break. The peak clad temperatures for the 4-inch break case is given in Figure 14.3-7b (the 2-inch break had no core uncovery).

The time sequence of events for small breaks analyzed is shown in Table 14.3-1, and Table 14.3-1a presents the results for these analyses.

14.3.2 MAJOR REACTOR COOLANT SYSTEM PIPE RUPTURES (LOSS-OF-COOLANT ACCIDENT)

The analysis specified by 10 CFR 50.46 (Reference 1) "Acceptance Criteria for Emergency Core Cooling Systems for Light Water Power Reactors", is presented in this section. The results of this analysis show compliance with the requirements of Appendix K of 10 CFR 50, which are described in the topical report, "Westinghouse ECCS Evaluation Model – Summary" (Reference 2).

For the purpose of ECCS analyses, Westinghouse (W) defines a large break loss-of-coolant accident (LOCA) as a rupture 1.0 ft^2 or larger of the reactor coolant system piping including the double ended rupture of the largest pipe in the reactor coolant system or of any line connected to that system. The boundary considered for loss of coolant accidents as related to connecting piping is defined in Section 4.1.3.

Should a major break occur, rapid depressurization of the Reactor Coolant System (RCS) occurs in approximately 20 seconds to a pressure nearly equal to the containment pressure, with a nearly complete loss of system inventory. Depressurization of the RCS results in a pressure decrease in the pressurizer. The reactor trip signal subsequently occurs when the pressurizer low-pressure trip setpoint is reached. However, since no credit is taken for rod insertion in the large break LOCA, the trip is not modeled in the analysis. A safety injection actuation signal is generated when the appropriate setpoint (high containment pressure or low pressurizer pressure) is reached. Although containment high pressure will actuate safety injection several tenths of a second before low pressurizer pressure, there is no way of modeling the containment high-pressure setpoint in the WCOBRA/TRAC large break LOCA Evaluation Model. These actions will limit the consequences of the accident in two ways:

1) Reactor trip and borated water injection complement void formation in causing rapid reduction of power to a residual level corresponding to fission product decay heat. The insertion of control rods to shut down the reactor is neglected in the large break LOCA analysis.

2) Injection of borated water provides for heat transfer from the core and prevents excessive cladding temperature.

Before the break occurs, the reactor is assumed to be in a full power equilibrium condition, i.e., the heat generated in the core is being removed through the steam generator secondary system. At the beginning of the blowdown phase, the entire RCS contains sub-cooled liquid which transfers heat from the core by forced convection with some fully developed nucleate boiling. During blowdown, heat from fission product decay, hot internals and the vessel, continues to be transferred to the reactor coolant. After the break develops, the time to departure from nucleate boiling is calculated. Thereafter, the core heat transfer is unstable, with both nucleate boiling and film boiling occurring. As the core becomes voided, both transition boiling and forced convection are considered as the dominant core heat transfer mechanisms. Heat transfer due to radiation is also considered.

The heat transfer between the RCS and the secondary system may be in either direction, depending on the relative temperatures. In the case of the large break LOCA, the primary pressure rapidly decreases below the secondary system pressure and the steam generators are an additional heat source. In the Kewaunee Nuclear Plant Large Break LOCA analysis using the WCOBRA/TRAC UPI methodology, the steam generator secondary is conservatively assumed to be isolated (main feedwater and steam line) at the initiation of the event to maximize the secondary side heat load.

When the RCS depressurizes to approximately 700 psig, the accumulators begin to inject borated water into the reactor coolant loops. Borated water from the accumulator in the faulted loop is assumed to spill to containment and be unavailable for core cooling for breaks in the cold leg of the RCS. Flow from the accumulator in the intact loop may not reach the core during depressurization of the RCS due to the fluid dynamics present during the ECCS bypass period. ECCS bypass results from the momentum of the fluid flow up the downcomer due to a break in the cold leg, which entrains ECCS flow out toward the break. Appendix K to 10 CFR 50 (Reference 1) requires conservative calculation of the end of the bypass period, as well as expulsion of all ECCS computed to inject prior to the end of bypass. Bypass of the ECCS diminishes as mechanisms responsible for the bypassing are calculated to be no longer effective.

The blowdown phase of the transient ends when the RCS pressure (initially assumed at 2280 psia) falls to a value approaching that of the containment atmosphere. After the end of the blowdown, refill of the reactor vessel lower plenum begins. Refill is completed when emergency core cooling water has filled the lower plenum of the reactor vessel, which is bounded by the bottom of the active fuel region of the fuel rods (called bottom of core (BOC) recovery time).

The re-flood phase of the transient is defined as the time period lasting from BOC recovery until the reactor vessel has been filled with water to the extent that the core temperature rise has been terminated. From the latter stage of blowdown and on into the beginning of re-flood, the intact loop accumulator tank rapidly discharges borated cooling water into the RCS. Although the portion injected prior to end of bypass is lost out the cold leg break, the accumulator eventually contributes to the filling of the reactor vessel downcomer. The downcomer water elevation head provides the driving force required for the re-flooding of the reactor core. The high head safety injection (HHSI) pump aids in the filling of the downcomer and core and subsequently supply water to help maintain a full downcomer and complete the re-flooding process. The low head safety injection (LHSI), which injects into the upper plenum (hence, upper plenum injection - UPI) also aids the re-flooding process by providing water to the core through the vessel upper plenum.

Continued operation of the ECCS pumps supplies water during long-term cooling. Core temperatures have been reduced to long-term steady state levels associated with dissipation of residual heat generation. After the water level of the refueling water storage tank (RWST) reaches a minimum allowable value, coolant for long-term cooling of the core is obtained by switching from the injection mode to the sump recirculation mode of ECCS operation. Spilled borated water is drawn from the engineered safety features (ESF)

containment sumps by the LHSI pumps (also called the Residual Heat Removal pumps, or RHR pumps) and returned to the upper plenum and RCS cold legs. Figure 14.3.2-1 contains a schematic of the bounding sequence of events for the Kewaunee large break LOCA transient.

Large Break LOCA Thermal Analysis

The reactor is designed to withstand thermal effects caused by a loss-of-coolant accident including the double-ended severance of the largest reactor coolant system pipe. The reactor core and internals together with the Emergency Core Cooling System are designed so that the reactor can be safely shut down and the essential heat transfer geometry of the core preserved following the accident. The Emergency Core Cooling System, even when operating during the injection mode with the most severe single active failure, is designed to meet the Acceptance Criteria (Reference 1).

The large break LOCA ECCS analysis applicable to Kewaunee with a full core of Siemens-Designed 14x14 Heavy fuel was performed with the Westinghouse WCOBRA/TRAC Two-Loop Upper Plenum Injection (UPI) Large Break LOCA Evaluation Model (References 3 and 4). The evaluation model and the major input parameters and initial conditions are described more fully in the following sections.

Large Break LOCA Evaluation Model

The analysis was performed using the Westinghouse Large Break LOCA Best-Estimate Methodology for plants which incorporate Upper Plenum Injection (UPI) in the Safety Injection System design (References 3 and 4). The Westinghouse Best-Estimate Methodology was developed consistent with guidelines set forth in the SECY-83-472 document (Reference 5). These guidelines provide for the use of realistic models and assumptions in the calculational framework. The technical basis for the use of this model is discussed in detail in References 3 and 4.

The SECY-83-472 document states that there are three areas of conservatism in the current licensing models: the required Appendix K conservatism, the conservatism added by both the NRC staff and industry to cover uncertainties, and the conservatism imposed by the industry in some cases to reduce the complexity of the analysis. Based on a review of the available experimental data and the best estimate computer code calculations, the NRC staff concluded that there is more than sufficient safety margin to assure adequate performance of the ECCS, and that this excess margin can be reduced without an adverse effect on plant safety. Therefore, in the SECY-83-472 approach, the NRC staff suggested that the licensee could utilize a realistic model of the PWR to calculate the plant response to a LOCA at the most realistic (50% probability), i.e., the Nominal Analysis, and at a more conservative 95% probability level, i.e., the Superbounded Analysis. The calculation at the 95% probability level would account for uncertainties in such things as power level, fuel initial temperature, nuclear parameters, and computer code uncertainties. The parameters, which imply uncertainty and the methods by which the uncertainties would be combined (either statistically or as a one-sided bias) would have to be justified. The uncertainty analyses can

be performed on a generic, realistic PWR model which is representative of a class of similar plants, that is, two-, three-, or four-loop PWRs so that generic uncertainties are applicable to the individual plants.

The WCOBRA/TRAC code uncertainty methodology calculation consists of two parts:

1) An assessment of the ability of WCOBRA/TRAC to model the PWR behavior (Reference 4), and

2) A quantified assessment of WCOBRA/TRAC capability to predict the measured quantities from various separate effects and systems effects experiments which cover the range of PWR accident conditions (Reference 3).

The sources of uncertainty within the code and the specific application of the code to the PWR calculation have been addressed in accordance with requirements of SECY-83-472. While performing this assessment it was determined that the uncertainty of several modeling effects could not be quantified by comparison to experimental data. Consequently, parametric sensitivity studies were performed which varied these modeling effects in the WCOBRA/TRAC computer code, and the uncertainty was determined based on the results of these sensitivity studies.

The numerical value for the code uncertainty was derived by comparing WCOBRA/TRAC to a wide range of experiments, which covered the expected range of conditions for the PWR. The uncertainty analysis considered the following items:

1) Code bias - obtained by comparing the code calculated temperatures to the average of temperatures measured from various single effects and integral tests.

2) The uncertainty in the code bias - the standard deviation of the code bias is calculated as δ_1

3) The uncertainty in the data for each of the experiments. The individual test data uncertainties are sample size weighted and pooled together to obtain a data uncertainty for all the experiments analyzed as δ_2.

4) The initial test condition uncertainty used in the WCOBRA/TRAC was assessed by examination of repeat experiments and is calculated as δ_3.

5) The test modeling uncertainty was assessed by performing noding sensitivity analyses on different tests and averaging the differences between the different cases, and is calculated as δ_4.

The uncertainty analysis was undertaken for both a blowdown and re-flood peak temperature. The code bias was a direct value added or subtracted from the calculated plant peak cladding temperature. The uncertainties from items 2 to 5 were statistically combined as the

square-root-sum-of-squares and raised to the 95th percentile by multiplying by 1.645. The equation for the plant peak cladding temperature at the 95th percentile becomes:

$$PCT_{P\geq 95\%} = PCT_{PLANT} + Code\ Bias + 1.645\sqrt{\delta_1^2 + \delta_2^2 + \delta_3^2 + \delta_4^2}$$

The nominal calculation is performed in the first application of UPI SECY WCOBRA/TRAC to a plant to provide assurance that the most probable PCT is below the estimate of the 95% probability value. However, the nominal calculation is itself a conservative estimate since several conservative assumptions are retained.

To demonstrate compliance with the specific requirements of Appendix K to 10 CFR 50 a third calculation is performed in which the plant-specific realistic best estimate calculation includes the required Appendix K features, such as 1971 ANS decay heat plus 20%, Moody break flow model, no return to nucleate boiling during blowdown, etc. The realistic calculation with the Appendix K required features is used to demonstrate compliance with the Acceptance Criteria of 10 CFR 50.46, provided that the peak cladding temperature exceeds the peak cladding temperature calculated at the 95% probability level but is below the Acceptance Criteria limit of 2200°F.

The Best Estimate UPI ECCS Evaluation Model is comprised of the WCOBRA/TRAC and COCO computer codes (References 3, 4 and 6). The WCOBRA/TRAC code is used to generate the complete transient (blowdown through re-flood) system hydraulics as well as the cladding thermal analysis.

WCOBRA/TRAC is the Westinghouse version of the COBRA/TRAC (Reference 7) code originally developed by Battelle Northwest Laboratory in the late 1970s. It is an advanced computer code used to simulate complex two-phase transient and steady state phenomena in nuclear reactors or other large complex heat exchange equipment.

WCOBRA/TRAC is a combination of two codes:

a) COBRA-TF, a 3-D, two-fluid, three-field model, capable of calculating complex flow fields in a wide variety of geometries.

b) TRAC-PD2, a 1-D, two-phase drift flux flow model used primarily to simulate piping systems.

The COBRA-TF computer code provides a transient, or steady state two-fluid, three-field representation of two-phase flow. Each field is treated in three dimensions and is compressible. Continuous vapor, continuous liquid and entrained liquid drops are the three fields. The conservation equations for each of the three fields and for heat transfer from the solid structures in contact with the fluid are solved using a semi-implicit, finite-difference numerical technique on an Eulerian mesh. The COBRA-TF vessel model features extremely flexible noding for both the hydrodynamic mesh and the heat transfer solution. The flexible

noding allows representation of single rod bundle sub-channel, or grouping of rod bundle sub-channels into larger hydrodynamic channels.

Multi-phase flows consisting of two or more fluids are separated by moving phase interfaces. In general, the phases can be present in any combination of liquid, solid, or gas. The flow pattern can assume any one of a wide variety of forms, such as bubbly flow, droplet flow, gas-particle flow, and stratified flow. Since the quantities of interest are the average behavior of each phase within the control volume, most work in multi-phase flow is done using average equations across the control volume.

The average conservation equations used in COBRA-TF are derived following the methods of Ishii (Reference 8). The average used is a simple Eulerian time average over a time interval, Δt, assumed to be long enough to smooth out the random fluctuations present in a multi-phase flow but short enough to preserve any gross unsteadiness in the flow. The resulting average equations can be formulated in either the mixture form or the two-fluid form. Due to its greater physical appeal and broader range of application, and the possibility of reduced uncertainty, the two-fluid approach is used as the foundation for COBRA-TF.

The two-fluid formulation uses a separate set of conservation equations and constitutive relations for each phase. The effects of one phase on another are accounted for by interfacial friction, heat and mass transfer interaction terms appearing in the equations. The conservation equations have the same form for each phase; only the constitutive relations and physical properties differ.

The three-field formulation used in COBRA-TF is a straightforward extension of the two-fluid model. The fields included are vapor, continuous liquid, and entrained liquid. Dividing the liquid phase into two fields is the most convenient and physically reasonable way of handling flows. For this representation of the liquid phase, the liquid can appear in both film and droplet form. This permits more accurate modeling of thermal-hydraulic phenomena such as entrainment, de-entrainment, fallback, liquid pooling and flooding.

One of the important features of the COBRA-TF vessel model is that the governing equations form a complete set. No terms are omitted particularly in the momentum equations where wall shear, momentum exchange due to turbulence and all the interfacial terms are represented. The COBRA-TF vessel model also has two energy equations to account for thermodynamic non-equilibrium between the two phases. This is particularly important for post CHF (dryout) conditions, where the vapor phase can be superheated and the liquid phase remains at the saturation temperature.

A complete set of heat transfer and flow regime models is incorporated into COBRA-TF. These models are applicable over a wide range of fluid and heat transfer conditions, as required by the range of conditions found during light water reactor transients. The flow regime model covers the full range from low-void fraction, bubbly regimes to highly dispersed droplet regimes and corresponding heat transfer models exist for

these flow regimes, for wall surface temperatures ranging from the fluid saturation temperature to approximately 3000°F.

WCOBRA/TRAC has been successfully utilized to analyze Westinghouse two-loop PWRs with Upper Plenum Injection (Reference 4). The results of these calculations indicate that the WCOBRA/TRAC analysis method verified the safety performance of the upper plenum injection system for this class of plants.

The system hydraulic transient is influenced by the containment pressure transient response to the mass and energy released from the reactor coolant system by the LOCA. In the Best Estimate UPI ECCS Evaluation Model, the containment pressure transient is provided as a boundary condition to the system hydraulic transient. The containment pressure transient applied is to be conservatively low and include the effect of the operation of all pressure reducing systems and processes. The COCO computer code (Reference 6) is used to generate the containment pressure response to the mass and energy release from the break. This containment pressure curve is then used as an input to the WCOBRA/TRAC code. It should be noted that high head and low head safety injection actuation is based on the pressurizer low pressure SI signal, and not on containment pressure high pressure SI signal. Although the latter is computed to occur earlier, it is conservative to model a later time for HHSI and LHSI injection. Additionally, since the WCOBRA/TRAC and COCO computer codes do not run interactively, it would be difficult to model HHSI and LHSI actuation on high containment pressure.

Large Break LOCA Input Parameters and Initial Conditions

Important input parameters and initial conditions used in the analysis are listed in Table 14.3.2-1. The assumptions made in the Appendix K and Superbounded cases respectively are listed in Tables 14.3.2-2 and 14.3.2-3. Note that the Nominal calculation was not performed in this analysis, since this is not the first-time application of UPI SECY WCOBRA/TRAC to the Kewaunee Nuclear Power Plant. The initial steady state fuel pellet temperature and fuel rod internal pressure used in the LOCA analysis were generated with the PAD 3.4 Fuel Rod Design Code (Reference 9) which has been approved by the USNRC. The fuel parameters input to the code were at beginning-of-life (maximum densification) values for the hot assembly and hot rod, and the remainder of the assemblies were modeled at a conservative value representative of average core burnup.

In determining the conservative direction for Superbounded values and assumptions for UPI plants, many sensitivity studies were performed (Reference 4). These sensitivities were performed using a representative two-loop plant with Upper plenum Injection (UPI) in the ECCS design. Since the representative two-loop plant has a higher peak linear heat rate and higher core power to pumped ECCS flow ratio than the Kewaunee Nuclear Power Plant, it will yield a greater change in peak cladding temperature for changes in plant parameters. These sensitivity studies were used to determine the direction of conservatism for choosing the bounding conditions for the 95th percentile calculation for the Kewaunee analysis.

The parameters used to determine the containment pressure curve are presented in Tables 14.3.2-4 through 14.3.2-7. Table 14.3.2-4 contains the containment data, Table 14.3.2-5 tabulates the fan cooler performance curve, and Table 14.3.2-6 lists the structural heat sinks in the containment. Table 14.3.2-7 contains the mass and energy releases to the containment for the Siemens Standard Fuel Appendix K calculation (Reference 10). The mass and energy releases from the Siemens Heavy Fuel Appendix K calculation described herein were compared with those of the Standard Fuel calculation, and were determined to be very similar. Any differences in the mass and energy flows would have a negligible effect on the containment pressure curve generated. Therefore, a new containment pressure curve was not generated. The containment pressure transient used to calculate the system hydraulic transient for the Siemens 14x14 Heavy Fuel Appendix K and Superbounded calculations is shown in Figure 14.3.2-2.

Initial conditions for the Kewaunee large break LOCA analysis are delineated in Table 14.3.2-1. Most of these parameters were chosen at their limiting values in order to provide a conservative bound for evaluation of the calculated peak cladding temperature for the large break LOCA analysis. The hot assembly was selected as an interior assembly under a support column surrounded by non-guide tube assemblies. Past sensitivity studies have demonstrated the limiting location for peak cladding temperature to be the limiting flow area at the upper core plate (Reference 4), and a support column has the limiting flow area among the various upper core plate geometries in Kewaunee.

Large Break LOCA Analysis Results

Two transient calculations are performed with WCOBRA/TRAC for Kewaunee with a full core of Siemens 14x14 Heavy fuel. The first is an Appendix K calculation that demonstrates the compliance with the specific requirements of Appendix K to 10 CFR 50. The second is the Superbounded calculation as per the approach defined in SECY-83-472. This calculation is performed at the 95% probability level and accounts for uncertainties in such things as power level, fuel initial temperature, and computer code uncertainties.

Appendix K Calculation Results

An Appendix K calculation was performed for the Kewaunee Nuclear Plant which conforms to the modeling requirements of 10 CFR 50.46 and Appendix K to 10 CFR 50. The conservative assumptions used in the Appendix K calculation are listed in Table 14.3.2-2. Table 14.3.2-8 shows the time sequence of events for the Appendix K calculation, and Table 14.3.2-9 provides a brief summary of the important results. Figure 14.3.2-3 contains the power shape used in the Appendix K calculation.

Shortly after the break is assumed to open, the vessel rapidly depressurizes (Figure 14.3.2-5) and the core flow quickly reverses. During the flow reversal, the hot assembly fuel rods dry out and begin to heat up momentarily (Figure 14.3.2-4).

At approximately 10 seconds into the transient, maximum blowdown downflow is reached in the high and low power regions of the core. Figures 14.3.2-12 through 14.3.2-19 show the

liquid flow (which is the sum of the liquid and entrained liquid flows) and the vapor flow at the top of the core assemblies[1]. This flow is sufficient to cool the hot assembly and average rods, and maintain the low power/periphery region of the core near the fluid saturation temperature (Figure 14.3.2-4).

As the vessel continues to depressurize, liquid inventory continues to be depleted (Figure 14.3.2-6). This results in reduced core flow, first in the hot assembly and soon after in the other assemblies (Figures 14.3.2-12 through 14.3.2-19) and subsequent cladding heatup, also first in the hot assembly, and later in the other regions of the core (Figure 14.3.2-4).

At approximately 7.5 seconds into the transient, the accumulator begins to inject water into the intact cold leg (Figure 14.3.2-9). This water begins to fill the cold leg and upper downcomer region (Figure 14.3.2-20), and is bypassed to the break initially. The end of bypass period occurs when flow in the core reverses for the last time during blowdown. The end of blowdown occurs at 20.58 seconds, and the downcomer and lower plenum begin to refill shortly thereafter (Figures 14.3.2-20 and 14.3.2-21). The lack of cooling in the core causes the core temperatures to continue to rise and the hot rod bursts due to high temperature creep at 25.67 seconds. The hot assembly also bursts at 40.56 seconds, which is just before the core begins to refill.

At approximately 27 seconds, the pressure in the vessel has dropped to near containment pressure (Figure 14.3.2-5), so accumulator and HHSI water begins to flow into the lower plenum from the downcomer (Figures 14.3.2-20 and 14.3.2-21), and the downcomer continues to refill. Also, at about 27 seconds, pumped high head safety injection (HHSI) into the cold leg begins (Figure 14.3.2-10). The bottom-of-core recovery time occurs at 30.17 seconds. The pumped low head safety injection (LHSI) into the upper plenum begins at approximately 32 seconds (Figure 14.3.2-11). Water from the LHSI flows down the low power/periphery region of the core (Figure 14.3.2-18) and cross-flows into the other core regions, providing some limited core cooling, and also fills the lower plenum. The inventory of the vessel begins to increase quickly as the lower plenum refills and break flow decreases to less than the HHSI and LHSI flows (Figures 14.3.2-6 through 14.3.2-11).

The accumulators empty at 33.30 seconds. At this time, the accumulator nitrogen injects into the cold leg and forces water down the downcomer and up through the core (Figures 14.3.2-12 through 14.3.2-19). The added flow causes the temperature in the assemblies to substantially decrease for a brief period (Figure 14.3.2-4). However, the

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[1] The core channels are defined as follows: Channel 10 is the "Open Hole / Support Column" Channel (OH/SC), which contains the 63 fuel assemblies located below open holes or support columns in the average power region of the core. Channel 11 is the "Guide Tube" Channel (GT), which contains the 33 fuel assemblies located below guide tubes in the average power region of the core. Channel 12 is the "Hot Assembly" Channel (HA), which contains the hottest assembly in the core (one rod of which is the "Hot Rod"), and is modeled as the limiting fuel assembly, a support column. Channel 13 is the "Low Power Region" Channel (LP), which contains the 24 fuel assemblies located in the periphery of the core that are at a lower power than the rest of the core.

increased flow through the core caused by the nitrogen injection forces water out the break (Figure 14.3.2-8), and also stops the downflow in the low power/periphery region of the core (Figure 14.3.2-18). The temperatures in the core assemblies therefore increase due to the lack of cross-flow from the low power/periphery region, and also the reduced vessel water inventory. As the nitrogen injection slows, the downflow in the low power/periphery region increases, and the ECCS water begins to refill the vessel once again (Figure 14.3.2-6).

At approximately 45 seconds, the lower plenum has filled to the point that water begins to re-flood the core from below (Figures 14.3.2-21 and 14.3.2-22). The void fraction in the lower plenum is reduced to nearly zero (Figure 14.3.2-23), and the void fractions at the bottom of the core are greatly reduced (Figure 14.3.2-24) as the core refills.

At approximately 82 seconds, sufficient cooling is provided by the safety injection system to begin cooling the core (Figure 14.3.2-4). At approximately 115 seconds, downcomer boiling begins to occur due to the stored energy in the vessel wall metal, and the water levels in the downcomer decrease (Figure 14.3.2-20) and the void fractions in the core channels increase (Figure 14.3.2-24). Boiling also occurs in the lower plenum, beginning at approximately 145 seconds (Figures 14.3.2-21 and 14.3.2-23). The cooling in the hot assembly decreases due to the reduced flow, and the peak-clad temperature increases (Figure 14.3.2-4).

At approximately 165 seconds, the downcomer begins to refill (Figure 14.3.2-20), and the vessel inventory slowly increases to an equilibrium state (Figure 14.3.2-6). At approximately 180 seconds, the amount of liquid in the core increases (Figure 14.3.2-22), providing cooling to the hot assembly and average assemblies, and the clad temperatures of all of the assemblies begin to decrease (Figure 14.3.2-4). Sufficient cooling is provided by the ECCS to continue cooling the core.

The peak cladding temperature calculated for the Kewaunee Nuclear Plant Appendix K large break LOCA analysis is shown in Table 14.3.2-9, and assumes a peak hot rod power of 14.661 kW/ft (based on an F_Q of 2.350 and a fraction of core power deposited in the fuel of 0.9615). This temperature occurs during re-flood at approximately 185 seconds. This result is below the acceptance criteria limit of 2200°F. The maximum local metal-water reaction is 3.3%, which is below the embrittlement Acceptance Criteria limit of 17%. The limiting total core metal-water reaction is 0.0033%, which is much less than the 1.0% limit, in accordance with the Acceptance Criteria. The clad temperature transient is terminated at a time when the core geometry is still amenable to cooling. As a result, the core temperature will continue to drop and the ability to remove decay heat generated in the fuel for an extended period of time will be provided.

Superbounded Calculation Results

A Superbounded calculation was performed at the 95% probability level for the Kewaunee Nuclear Plant as per the SECY-83-472 approach. The assumptions used in the Superbounded calculation are listed in Table 14.3.2-3. Table 14.3.2-8 shows the time sequence of events for the Superbounded calculation, and Table 14.3.2-9 provides a brief summary of the important results.

The Superbounded transient is similar to the Appendix K transient described above. The same figures have been provided as in the Appendix K results.

Most of the differences are timing issues. In general, the events occur earlier in the Superbounded calculation, as can be seen in Table 14.3.2-8, since the break discharge coefficient, C_D, is larger in the Superbounded calculation than in the Appendix K calculation, i.e., 0.6 versus 0.4. Therefore, the blowdown peak clad temperature occurs earlier in the Superbounded calculation, as does the end of the blowdown portion of the transient.

The vessel depressurizes more rapidly, allowing the accumulator and HHSI and LHSI flows to begin earlier, and the core begins to refill earlier. The accumulators inject faster since the system pressure is lower, and therefore are empty sooner than in the Appendix K calculation.

Nitrogen injection is not modeled in the Superbounded calculation, so the dip in peak clad temperature due to the nitrogen in the Appendix K calculation is not seen in the Superbounded calculation, and the cladding continues to heat up (Figure 14.3.2-25).

The peak cladding temperature calculated for the Kewaunee Nuclear Plant Superbounded large break LOCA analysis is shown in Table 14.3.2-9, and assumes a peak hot rod power of 14.661 kW/ft (which is equivalent to an Appendix K peak hot rod power of 16.127 kW/ft). This temperature occurs during blowdown at approximately 7.0 seconds, and includes 260.5°F added for uncertainties (Reference 3, Addendum 4). This result is below the Appendix K result, as required by SECY-83-472 (Reference 5). The maximum local metal-water reaction is less than 17%, and the limiting total core metal-water reaction is less than the 1.0% limit, in accordance with the Acceptance Criteria. By the end of the transient, the core temperature has dropped to the saturation temperature, and all of the rods modeled have quenched. The clad temperature transient is terminated at a time when the core geometry is still amenable to cooling. Therefore, the ability to remove decay heat generated in the fuel for an extended period of time will be provided.

Transition Core Effects Evaluation

The peak clad temperature penalty due to the transition from Siemens 14x14 Standard fuel to Siemens 14x14 Heavy fuel must be evaluated for the effects of hydraulic mismatch and differences in fuel designs. The Superbounded calculation is used to determine the effect of the transition core. If a penalty is determined in the evaluation, the penalty is then applied to the Appendix K calculation peak clad temperature.

There is no transition core effect due to hydraulic differences between the two assembly types, since the clad outer diameters are the same in the two Siemens fuel assemblies, the grid spacers are located at the same elevations, and the losses through the core are approximately the same. Therefore, only the effect of the differences in initial fuel temperatures, rod internal pressures, and fuel dimensions must be considered in evaluating the transition core effects.

To determine the effect of the initial fuel temperatures, rod internal pressures, and other fuel characteristics, a Superbounded calculation is performed in which the hot assembly is modeled as a fresh assembly of Siemens 14x14 Heavy fuel, and the remainder of the assemblies are modeled as the once-burned (or more) Siemens 14x14 Standard fuel. The minimum fuel rod burnup of the once-burned Siemens Standard Fuel must be more than 3530 MWD/MTU and the minimum assembly average burnup must be more than 8000 MWD/MTU.

A comparison plot of the PCT of the two Superbounded calculations, i.e., the full core of Siemens Heavy Fuel and the transition core calculation, is provided in Figure 14.3.2-46. Similarly, comparison plots of the clad temperatures in the guide tube (GT) assemblies and open hole / support column (OH/SC) assemblies are provided in Figures 14.3.2-47 and 14.3.2-48, respectively.

There is only a slight penalty during blowdown due to the transition core effects, and a benefit during re-flood. However, as expected, since the initial fuel temperatures of the average and low power assemblies are higher in the transition core case than in the Heavy Fuel case, the clad temperatures of the GT and OH/SC assemblies are higher during blowdown (more stored energy), and during early re-flood, when the PCT of those assemblies occur, and also remain at higher temperatures later in the transient and quench later. The higher temperatures in the average rods generate more steam, which forces more water into the Hot Assembly, providing more cooling to the Hot Rod.

The calculations with a full core of Siemens 14x14 Heavy fuel result in a less severe large break LOCA transient than calculations with a full core of Siemens 14x14 Standard fuel. Therefore, consideration was also given to the instance where the hot assembly was a once-burned Siemens Standard fuel assembly at less than the minimum assembly average burnup, and the hot rod was at less than the minimum rod burnup. It was determined that the calculation with the hot assembly modeled with Heavy fuel at beginning-of-life burnup bounded the Standard fuel at the minimum burnup for once-burned fuel. This transition core calculation therefore covers all possible loading patterns during the transition from Siemens 14x14 Standard fuel to Siemens 14x14 Heavy fuel.

Therefore, since the Appendix K calculation is re-flood limited, and the transition core shows that there is a PCT benefit during re-flood (in the Hot Assembly), no transition core penalty is assigned to the Appendix K calculation.

Evaluation of the Low Power Region Factor

An evaluation was performed to determine the effect of increasing the Low Power Region Power Factor from 0.45 to 0.60 on the Siemens Standard Fuel analysis, while taking credit for 1500 MWD/MTU of cycle burnup.

The evaluation showed that the benefit gained in core stored energy due to the fuel temperature reduction at a cycle burnup of 1500 MWD/MTU is greater than the penalty from the increased Low Power Region Power Factor (P_{LOW}). Therefore, the analysis performed

modeling Siemens 14x14 Heavy Fuel and 30% SGTP is valid for a fuel reload cycle, with the following restrictions:

1) The average power fraction of the assemblies in the low power region of the core (P_{LOW}) must remain below 0.45 for the first 1500 MWD/MTU of cycle burnup.

2) After 1500 MWD/MTU of cycle burnup, P_{LOW} must remain below 0.60.

Large Break LOCA Analysis Conclusions

This large break LOCA analysis has determined that Kewaunee operation with Siemens-Designed 14x14 Heavy fuel and the transition from 14x14 Standard fuel to 14x14 Heavy fuel is acceptable. The large break LOCA analysis presented in this section shows that the high head and low head safety injection, together with the accumulators, provide sufficient core flooding to meet the 10 CFR 50.46 Acceptance Criteria. That is:

1) The calculated peak fuel element cladding temperature does not exceed 2200°F.

2) The calculated total oxidation of the cladding nowhere exceeds 17% of the thickness before oxidation.

3) The calculated total amount of hydrogen generated from the chemical reaction of the cladding with water or steam does not exceed 1.0% of the hypothetical amount that would be generated if all of the metal in the cladding cylinders surrounding the fuel were to react.

4) The core remains amenable to cooling.

5) The core temperature is maintained at an acceptably low value and decay heat is removed for the extended period of time required for the long-lived radioactivity remaining in the core.

14.3.3 CORE AND INTERNALS INTEGRITY ANALYSIS

The response of the reactor core and vessel internals under excitation produced by a simultaneous complete severance of a reactor coolant pipe and seismic excitation of typical two loop plant internals has been determined. A detailed description of the analysis applicable to the Kewaunee Nuclear Power Plant design appears in WCAP 7822, (Reference 1) Indian Point Unit 2 Reactor Internals Mechanical Analysis for Blowdown Excitation (Westinghouse Proprietary).

Reactor Internals Response Under Blowdown and Seismic Excitation

A loss-of-coolant accident may result from a rupture of reactor coolant piping. During the blowdown of the coolant, critical components of the core are subjected to vertical and horizontal excitation as a result of rarefaction waves propagating inside the reactor vessel.

For these large breaks, the reduction in water density greatly reduces the reactivity of the core, thereby shutting down the core whether the rods are tripped or not. (The subsequent refilling of the core by the Emergency Core Cooling System uses borated water to maintain the core in a sub-critical state.) Therefore, the main requirement is to assure effectiveness of the Emergency Core Cooling System. Insertion of the control rods, although not needed, gives further assurance of ability to shut the plant down and keep it in a safe shutdown condition.

The pressure waves generated within the reactor are highly dependent on the location and nature of the postulated pipe failure. In general, the more rapid the severance of the pipe, the more severe the imposed loadings on the components. A one-millisecond severance time is taken as the limiting case.

In the case of the hot leg break, the vertical hydraulic forces produce an initial upward lift of the core. A rarefaction wave propagates through the reactor hot leg nozzle into the interior of the upper core barrel. Since the wave has not reached the flow annulus on the outside of the barrel, the upper barrel is subjected to an impulsive compressive wave. Thus, dynamic instability (buckling) or large deflection of the upper core barrel or both is the possible response of the barrel during hot leg blowdown. In addition to the above effects, the hot leg break results in transverse loading on the upper core components as the fluid exits the hot leg nozzle.

In the case of the cold leg break, a rarefaction wave propagates along a reactor inlet pipe arriving first at the core barrel at the inlet nozzle of the broken loop. The upper barrel is then subjected to a nonaxisymmetric expansion radial impulse, which changes as the rarefaction wave propagates both around the barrel and down the outer flow annulus between vessel and barrel. After the cold leg break, the initial steady-state hydraulic lift forces (upward) decrease rapidly (within a few milliseconds) and then increase in the downward direction. These cause the reactor core and lower support structure to move initially downward.

If a simultaneous seismic event with the intensity of the design basis earthquake (DBE) is postulated with the loss-of-coolant accident, the imposed loading on the internals component maybe additive in certain cases, and therefore, the combined loading must be considered. In general, however, the loading imposed by the earthquake is small compared to the blowdown loading.

Acceptance Criteria for Results of Analyses

The criteria for acceptability in regard to mechanical integrity analysis is that adequate core cooling and core shutdown must be assured. This implies that the deformation of the reactor internals must be sufficiently small so that the geometry remains substantially intact. Consequently, the limitations established on the internals are concerned principally with the maximum allowable deflections and/or stability of the parts, in addition to a stress criterion, to assure integrity of the components.

Allowable Deflection and Stability Criteria

Upper Barrel - The upper barrel deformation has the following limits:

a) To insure a shutdown and cooldown of the core during blowdown, the basic requirement is a limitation on the outward deflection of the barrel at the locations of the inlet nozzles connected to the unbroken lines. A large outward deflection of the barrel in front of the inlet nozzles, accompanied with permanent strains, could close the inlet area and stop the cooling water coming from the accumulators. (The remaining distance between the barrel and the vessel inlet nozzle after the accident must be such that the inlet flow area be approximately the same as that of the accumulator pipes). Consequently, a permanent barrel deflection in front of the unbroken inlet nozzles larger than a certain limit called the "no-loss of function" limit, could impair the efficiency of the Emergency Core Cooling System.

b) To assure rod insertion and to avoid disturbing the Control Rod Cluster guide structure, the barrel should not interfere with the guide tubes. This condition also requires a stability check to assure that the barrel will not buckle under the accident loads.

Control Rod Cluster Guide Tubes - The guide tubes in the upper core support package house the control rods. The deflection limits were established from tests.

Fuel Assembly - The limitations for this case are related to the stability of the thimbles in the upper end. The upper end of the thimbles shall not experience stresses above the allowable dynamic compressive stresses. Any buckling of the upper end of the thimbles due to axial compression could distort the guideline and thereby affect the free fall of the control rod.

Upper Package – The maximum allowable local deformation of the upper core plate where a guide tube is located is 0.100 inch. This deformation will cause the plate to contact the guide tube since the clearance between plate and guide tube is 0.100 inches. This limit will prevent the guide tubes from undergoing compression. For a plate local deformation of 0.150 inches, the guide tube will be compressed and deformed transversely to the upper limit previously established; consequently, the value of 0.150 inches is adopted as the no loss-of-function local deformation, with an allowable limit of 0.100 inches.

Allowable Stress Criteria

For this faulted condition, the allowable stress criteria is given by Figure 14.3-21a. This figure defines various criteria based upon their corresponding method of analysis.

To account for multi-axial stresses, the Von Mises Theory is also considered.

Method of Analysis

Blowdown Model

BLODWN-2 is a digital computer program developed for the purpose of calculating local fluid pressure, flow, and density transients that occur in PWR coolant systems during a loss-of-coolant accident (Reference 2). This program applies to the sub-cooled, transition, and saturated two-phase blowdown regimes. BLODWN-2 is based on the method of characteristics wherein the resulting set of ordinary differential equations, obtained from the laws of conservation of mass, momentum, and energy, are solved numerically using a fixed mesh in both space and time.

Although spatially one-dimensional conservation laws are employed, the code can be applied to describe three-dimensional system geometries by use of the equivalent piping networks. Such piping networks may contain any number of pipes or channels of various diameters, dead ends, branches (with up to six pipes connected to each branch), contractions, expansions, orifices, pumps, and free surfaces (such as in the pressurizer). System losses such as friction, contraction, expansion, etc., are considered.

BLODWN-2 predictions have been compared with numerous test data as reported in WCAP-7401 (Reference 4). It is shown that the BLODWN-2 digital computer program correlates well with both the sub-cooled and the saturated blowdown regimes.

FORCE Model for Blowdown

BLODWN-2 evaluates the pressure and velocity transients for a maximum of 2400 locations throughout the system. These pressure and velocity transients are stored as a permanent tape file and are made available to the program FORCE which utilizes a detailed geometric description in evaluating the loadings on the reactor internals.

Each reactor component for which FORCE calculations are required is designated as an element and assigned an element number. Forces acting upon each of the elements are calculated summing the effects of:

1) The pressure differential across the element.

2) Flow stagnation on, and unrecovered orifice losses across the element.

3) Friction losses along the element.

Input to the code, in addition to the BLODWN-2 pressure and velocity transients includes the effective area of each element on which the force acts, due to the pressure differential across the element, a coefficient to account for flow stagnation, unrecovered orifice losses, and the total area of the element along which the shear forces act.

The mechanical analysis has been performed using conservative assumptions in order to obtain results with extra margin. Some of the most significant are:

a) The mechanical and hydraulic analysis has been performed separately without including the effect of the water-solid interaction. Peak pressures obtained from the hydraulic analysis will be attenuated by the deformation of the structures.

b) When applying the hydraulic forces, no credit is taken for the stiffening effect of the fluid environment, which will reduce the deflections and stresses in the structure.

c) The multi-mass model described below is considered to have a sufficient number of degrees of freedom to represent the most important modes of vibration in the vertical direction. This model is conservative in the sense that further mass-spring resolution of the system would lead to further attenuation of the shock effects obtained with the present model.

Vertical Excitation Model for Blowdown

For the vertical excitation, the reactor internals are represented by a multi-mass system connected with springs and dashpots simulating the elastic response and the viscous damping of the components. Also incorporated in the multi-mass system is a representation of the motion of the fuel elements relative to the fuel assembly grids. The fuel elements in the fuel assemblies are kept in position by friction forces originating from the preloaded fuel assembly grid fingers. Coulomb-type friction is assumed in the event that sliding between the rods and the grid fingers occurs. Figure 14.3-21 shows the spring-mass system used to represent the internals. In order to obtain an accurate simulation of the reactor internals response, the effects of internal damping, clearances between various internals, snubbing action caused by solid impact, Coulomb friction induced by fuel rods motion relative to the grids, and pre-loads in holddown springs have been incorporated in the analytical model. The reactor vessel is regarded as a fixed base while the internals undergo relative displacement with respect to their initial position. The modeling is conducted in such a way that uniform masses are lumped into easily identifiable discrete masses while elastic elements are represented by springs. Table 14.3-3 lists the various masses, springs, etc.

The appropriate dynamic differential equations for the multi-mass model describing the aforementioned phenomena are formulated and the results obtained using a digital computer program which computes the response of the multi-mass model when excited by a set of time dependent forcing functions. The appropriate forcing functions are applied simultaneously and independently to each of the masses in the system. The results from the program give the forces, displacements and deflections as functions of time for all the reactor internals components (lumped masses). Reactor internals response to both hot and cold leg pipe ruptures are analyzed. The forcing functions used in the study are obtained from hydraulic analyses of the pressure and flow distribution around the entire reactor coolant system as caused by double-ended severance of a reactor coolant system pipe.

Vertical Excitation Model for Earthquake

As shown in WCAP-7822 (Reference 1) the reactor internals are modeled as a single degree-of-freedom system for vertical earthquake analysis. The maximum acceleration at the vessel support is increased by amplification due to the building-soil interaction.

Transverse Excitation Model for Blowdown

Various reactor internal components are subjected to transverse excitation during blowdown. Specifically, the barrel, guide tubes, and upper support columns are analyzed to determine their response to this excitation.

Core Barrel

For the hydraulic analysis of the pressure transients during hot leg blowdown, the maximum pressure drop across the barrel is a uniform radial compressive impulse. The barrel is then analyzed for dynamic buckling using these conditions and the following conservative assumptions:

a) The effect of the fluid environment is neglected (water stiffening is not considered);

b) The shell is treated as simply supported.

During cold leg blowdown, the upper barrel is subjected to a nonaxisymmetric expansion radial impulse, which changes as the rarefaction wave propagates both around the barrel and down the outer flow annulus between vessel and barrel.

The analysis of transverse barrel response to cold leg blowdown is performed as follows:

1) The upper core barrel is treated as a simply supported cylindrical shell of constant thickness between the upper flange weldment and the lower core barrel weldment without taking credit for the supports at the barrel mid-span offered by the outlet nozzles. This assumption leads to conservative deflection estimates of the upper core barrel.

2) The upper core barrel is analyzed as a shell with four variable sections to model the support flange, upper barrel, reduced weld section, and a portion of the lower core barrel.

3) The barrel with the core and thermal shield is analyzed as a beam fixed at the top and elastically supported at the lower radial support and the dynamic response is obtained.

Guide Tubes - The dynamic loads on RCC guide tubes are more severe for a loss-of-coolant accident caused by hot leg rupture than for an accident by cold leg rupture since the cold leg break leads to much smaller changes in the transverse coolant flow over the RCC guide tubes. Thus, the analysis is performed only for a hot leg blowdown.

The guide tubes in closest proximity to the ruptured outlet nozzle are the most severely loaded. The transverse guide tube forces during the hot leg blowdown decrease with increasing distance from the ruptured nozzle location.

A detailed structural analysis of the RCC guide tubes was performed to establish the equivalent cross-section properties and elastic and support conditions. An analytical model was verified both dynamically and statically by subjecting the control rod cluster guide tube to a concentrated force applied at the transition plate. In addition, the guide tube was loaded experimentally using a triangular distribution to conservatively approximate the hydraulic loading. The experimental results consisted of a load deflection curve for the RCC guide tube plus verification of the deflection criteria to assure RCC insertion.

The response of the guide tubes to the transient loading due to blowdown may be found by utilizing the equivalent single freedom system for the guide tube using experimental results for equivalent stiffness and natural frequency.

The time dependence of the hydraulic transient loading has the form of a step function with constant slope front with a rise time to peak force of the same order of the guide tube fundamental period in water. The dynamic application factor in determining the response is a function of the ramp impulse rise time divided by the period of the structure.

Upper Support Columns - Upper support columns located close to the broken nozzle during hot leg break will be subjected to transverse loads due to cross flow.

The loads applied to the columns were computed with a similar method to the one for the guide tubes, i.e., taking into consideration the increase in flow across the column during the accident. The columns were studied as beams with variable section and the resulting stresses were obtained using the reduced section modulus at the slotted portions.

Transverse Excitation Model for Earthquake

The reactor building with the reactor vessel support, the reactor vessel, and the reactor internals are included in this analysis. The mathematical model of the building, attached to ground, is identical to that used to evaluate the building structure. The reactor internals are mathematically modeled by beams, concentrated masses and linear springs.

All masses, water and metal are included in the mathematical model. All beam elements have the component weight or mass distribution uniformly, e.g., the fuel assembly-mass and barrel mass. Additionally, wherever components are attached uniformly their mass is included as an additional uniform mass, e.g., baffles and formers acting on the core barrel. The water near and about the beam elements is also included as a distributed mass. Horizontal components are considered as a concentrated mass acting on the barrel. This concentrated mass also includes components attached to the horizontal members, since these are the media through which the reaction is transmitted. The water near and about these separated components is considered as being additive at these concentrated mass points.

The concentrated masses attached to the barrel represent the following:

- the upper core support structure, including the upper vessel head and one-half the upper internals;
- the upper core plate, including one-half the thermal shield and the other one-half of the upper internals;
- the lower core plate, including one-half of the lower core support columns;
- the lower one-half of the thermal shield, and
- the lower core support, including the lower instrumentation and the remaining half of the lower core support columns.

The modulus of elasticity is chosen at its hot value for the three major materials found in the vessel, internals, and fuel assemblies. In considering shear deformation, the appropriate cross-sectional area is selected along with a value for Poisson's ratio. The fuel assembly moment of inertia is derived from experimental results by static and dynamic tests performed on fuel assembly modes. These tests provide stiffness values for use in this analysis.

The fuel assemblies are assumed to act together and are represented by a single beam. The following assumptions are made in regard to connection restraints. The vessel is pinned to the vessel support and part of the containment building. The barrel is clamped to the vessel at the barrel flange and spring-connected to the vessel at the lower core barrel radial support. This spring corresponds to the radial support stiffness for two opposite supports acting together. The beam representing the fuel assemblies is pinned to the barrel at the locations of the upper and lower core plates.

The response spectrum method has been used in the calculation. After computing the transverse natural frequency and obtaining the normal modes of the complete structure, the maximum response is obtained from the superposition of the usual mode response with the conservative assumptions that all the modes are in phase and that all the peaks occur simultaneously.

Conclusions - Mechanical Analysis

The results of the analysis applicable to the Kewaunee design are presented in Table 14.3-3a and Table 14.3-3b. These tables summarize the maximum deflections and stresses for blowdown, seismic, and blowdown plus seismic loadings.

The stresses due to the DBE (vertical and horizontal components) were combined in the most unfavorable manner with the blowdown stresses in order to obtain the largest principal stress and deflection.

These results indicate that the maximum deflections and stress in the critical structures are below the established allowable limits. For the transverse excitation, it is shown that the upper barrel does not buckle during a hot leg break and that it has an allowable stress distribution during a cold leg break. Section 5.9, under Primary Piping, discusses the

restraints which were added to restrain the reactions of jet forces in the primary loop piping caused by pipe rupture, to limit the LOCA loads.

Even though control rod insertion is not required for plant shutdown, this analysis shows that most of the guide tubes will deform within the limits established experimentally to assure control rod insertion with the exceptions shown in Table 14.3-3a. It can be seen in the Table that 31 of the 33 guide tubes are below the N.L.F. limit. For those guide tubes deflected above the N.L.F. limit, it must be assumed that the rods will not drop. However, the conclusion reached is that the core will shutdown in an orderly fashion due to the formation of voids, and this orderly shutdown will be aided by the great majority of rods that do drop.

14.3.4 CONTAINMENT INTEGRITY EVALUATION

Method of Analysis

Calculation of containment pressure and temperature transients is accomplished by use of the digital computer code, COCO. The analytical model is restricted to the containment volume and structure. Transient phenomena within the Reactor Coolant System affect containment conditions by means of convective mass and energy transport through the pipe break.

For analytical rigor and convenience, the containment air steam-water mixture is separated into two systems. The first system consists of the air-steam phase, while the second is the water phase. Sufficient relationships to describe the transient are provided by the equations of conservation of mass and energy as applied to each system, together with appropriate boundary conditions. As thermodynamic equations of state and conditions may vary during the transient, the equations have been derived for all possible cases of superheated or saturated steam, and sub-cooled or saturated water. Switching between states is handled automatically by the code. The following are the major assumptions made in the analysis:

a) Discharge mass and energy flow rates through the Reactor Coolant System break are established from the coolant blowdown and core thermal transient analysis (described in the preceding paragraphs).

b) At the break point, the discharge flow separates into steam and water phases. The saturated water phase is at the total containment pressure, while the steam phase is at the partial pressure of the steam in the containment.

c) Homogeneous mixing is assumed. The steam-air mixture and the water phase have uniform properties. More specifically, thermal equilibrium between the air and steam is assumed. This does not imply thermal equilibrium between the steam-air mixture and the water phase.

d) Air is taken as an ideal gas, while compressed water and steam tables are employed for water and steam thermodynamic properties.

During the transient, there is energy transfer from the steam, air and water systems to the internal structures and equipment within the shell.

Provision is made in the computer analysis for the effects of several engineered safety features, including internal spray, fan coolers, and recirculation of sump water. The heat removal from containment steam-air phase by internal spray is determined by allowing the spray water temperature to rise to the steam-air temperature.

Energy Sources

The amount of mass and energy carried into the containment during blowdown is calculated by SATAN computer code as adjusted to reflect conservatively high core heat transfer coefficients. The following is a summary of all the energy sources potentially available for transfer to the containment for a loss-of-coolant accident.

- Reactor Coolant Energy
- Accumulator Energy (Mixes with Reactor Coolant System)
- Initial Core Stored Energy
- Core Internals Metal Energy
- Reactor Vessel Metal (below vessel nozzles)
- Core Power Generation (shutdown energy and decay heat)
- Zr - H_2O reaction (Includes recombination energy)

All the initial core stored energy and the power generated by the core during blowdown is available for transfer to the coolant, and hence, to the containment. The initial metal sensible energy is transferred to the coolant by a time-dependent temperature difference calculation. It should be emphasized that the energy transferred from the core to the coolant for the containment evaluation far exceeds that transferred from the core thermal evaluation. That is to say, a conservatively high core heat transfer coefficient is used for the containment evaluation, while a conservatively low coefficient is used during core thermal evaluation.

Any energy addition resulting from a Zr - H_2O reaction is also considered. The reaction energy reaches the containment by transfer to the coolant. The recombination energy of the H_2 generated in the reaction is added directly to the steam-air mixture in the containment. The hydrogen is assumed to burn as it is produced.

Finally, hot metal surfaces not cooled by safety injection water (reactor vessel above nozzles and steam generator tubes) are simulated as hot walls in contact with the containment steam-air mixture. A small heat transfer coefficient is employed to reflect actual conditions since these surfaces are covered by stagnant steam inside the Reactor Coolant System.

The following are some additional conservative assumptions used in the analysis:

a) The reactor power is based on operation at the maximum calculated power of 1721.4 MWt, which is 4.3% greater than the application at 1650 MWt.

b) The decay heat is based on power operation for an infinite time.

c) Coolant temperatures are the maximum levels attained in steady-state operation, including allowance for instrument error and deadband.

d) Gross system volumes are calculated from component dimensions, to which is added a 3% margin.

e) Pressurizer liquid inventory at the nominal full-power level plus an appropriate margin for instrument error and deadband.

Energy Sinks

Containment Structures

Provision is made in the containment pressure transient analysis for heat transfer through, and heat storage in, both interior and exterior walls. Every wall is divided into a large number of nodes. For each node, a conservation of energy equation expressed in finite difference form accounts for transient conduction into and out of the node and temperature rise of the node. Table 14.3-4 is a summary of the containment structural heat sinks used in the analysis.

The heat transfer coefficient to the containment surface is calculated by the computer program, based primarily on the work of Tagami (Reference 1). From this work it was determined that the value of the heat transfer coefficient increases parabolically to peak value at the end of blowdown and then decreases exponentially to a stagnant heat transfer coefficient which is a function of steam to air weight ratio.

Tagami presents a plot of the maximum value of has a function of "coolant energy transfer speed", defined as:

$$\frac{\text{total coolant energy transferred into containment}}{\text{(containment vessel volume) (time interval to peak pressure)}}$$

From this the maximum of h for steel is calculated:

where:

$$h_{max} = 75\left[\frac{E}{t_p v}\right]^{0.60}$$

h_{max}	=	Maximum value of h (Btu/hr Ft² °F)
t_p	=	Time from start of accident to end of blowdown (sec)
V	=	Containment volume (Ft³)
E	=	Coolant energy discharged (Btu)

The parabolic increase to the peak value is given by:

where:

$$h_s = h_{\max}\sqrt{\frac{t}{t_p}} \quad 0 \le t \le t_p$$

hs = Heat transfer coefficient for steel (Btu/hr ft² °F)
t = Time from start of accident (sec)

The exponential decrease of the heat transfer coefficient is given by:

$$h_s = h_{stag} + \left(h_{\max} - h_{stag}\right) e^{-.05\left(t - t_p\right)} \quad t > t_p$$

where:

h_{stag} = h for stagnant conditions (Btu/hr ft² °F)
= $2 + 50\chi$ $\quad 0 \le X \le 1.4$
X = Steam-to-air weight ration in containment

For concrete the heat transfer coefficient is taken as 40% of the value calculated for steel.

Air Cooling Fan Coils

The ability of the Reactor Containment Vessel air recirculatory coolers to function properly in the accident environment is demonstrated by tests and analysis documented and referenced in the Fan Coil Unit Design Report submitted as Amendment No. 11 to the initial license application (Reference 2). The ability of the fan coils to perform under normal and accident conditions is documented in the manufacturer's design report and coil performance analyses (References 7, 8, 9, and 10).

The fan-cooler heat removal rates per fan are presented in Figure 14.3-22.

Containment Spray

When a spray drop enters the hot saturated steam-air environment, the vapor pressure of the water at its surface is much less than the partial pressure of the steam in the atmosphere. Hence, there will be diffusion of steam to the drop surface and condensation on the drop. This mass flow will carry energy to the drop. Simultaneously, the temperature difference between the atmosphere and the drop will cause a heat flow to the drop. Both of these mechanisms will cause the drop temperature and vapor pressure to rise. The vapor pressure of the drop will eventually become equal to the partial pressure of the steam and the condensation will cease. The temperature of the drop will be essentially equal to the temperature of the steam-air mixture.

The terminal velocity of the drop can be calculated using the formula given by Weinberg (Reference 3) where the drag coefficient C_D is a function of the Reynolds number:

$$V2 = \frac{4Dg^{(p-pm)}}{3C_D^{p}m}$$

For the 700 micron drop size expected from the nozzles, the terminal velocity is less than 7 ft/sec. For a 1000-micron drop, the velocity would be less than 10 ft/sec. The Nusselt number for heat transfer, Nu, and the Nusselt number for mass transfer, Nu′ (Sherwood Number), can be calculated from the empirical relations given by Ranz and Marshal (Reference 4).

$$Nu = 2+0.6(\mathrm{Re})^{1/2}(\mathrm{Pr})^{1/3}$$
$$Nu' = 2+0.6(\mathrm{Re})^{1/2}(Sc)^{1/3}$$

The Prandtl number and the Schmidt number for the conditions assumed are approximately 0.7 and 0.6, respectively. Both of these are sufficiently independent of pressure, temperature and composition to be assumed constant under containment conditions (References 5 and 6). The coefficients of heat transfer (h_c) and mass transfer (k_G) are calculated from Nu and Nu′, respectively. The equations describing the temperature rise of a falling drop are:

$$\frac{d}{dt}(Mu) = mh_g + q; \qquad \frac{d}{dt}(M) = m$$

where:

$$q = h_c A(T_s - T)$$
$$m = k_g A(P_s - P_v)$$

These equations can be integrated numerically to find the internal energy and mass of the drop as a function of time as it falls through the atmosphere. Analysis shows that the liquid drop temperature rises to the steam-air mixture temperature in less than 0.5 seconds, which occurs before the drop has fallen 5 feet. These results demonstrate that the spray will be 100% effective in removing heat from the atmosphere.

Nomenclature

A	=	Area
C_D	=	Drag coefficient
D	=	Droplet diameter
g	=	Acceleration of gravity

h_g	=	Coefficient of heat transfer
h_s	=	Steam Enthalpy
k_G	=	Coefficient of mass transfer
M	=	Droplet mass
m	=	Diffusion rate
Nu	=	Nusselt number for heat transfer
Nu´	=	Nusselt number for mass transfer
P_s	=	Steam partial pressure
P_v	=	Droplet vapor pressure
Pr	=	Prandtl number
q	=	Heat flow rate
Re	=	Reynolds number
Sc	=	Schmidt number
T	=	Droplet temperature
T_s	=	Steam temperature
t	=	Time
u	=	Droplet external energy
V	=	Velocity
p	=	Droplet density
pm	=	Steam-air mixture density

Containment Pressure Transient

The containment pressure was calculated for a range of large area ruptures of the Reactor Coolant System. The rupture sizes considered were:

- Double Ended Rupture
- 4.5ft² break
- 3ft² break
- 0.5ft² break

Figure 14.3-23 presents the results of the transients. For all cases a pressure peak of less than 42.2 psig was calculated. Since the design pressure for the Kewaunee plant is 46 psig, a margin more than 9% above the conservative value of the blowdown peak is available.

In the transients, one spray pump and two fans starting at 60 seconds were assumed. These acted to quickly reduce the pressure after the peak pressures were reached. The spray pump provides 1300 gpm of spray flow at containment design pressure. After the refueling water storage tank is exhausted, containment spray is continued. One-of-two residual heat removal loops is utilized to recirculate and cool water from the containment sump, providing a source of cooled water for the containment spray and safety injection functions.

The following paragraphs are a summary of the energy sources and sinks used in the calculation.

Energy Sources

The energy sources presented in Table 14.3-5 are potentially available to be transferred to the containment during the blowdown time.

In the above energy summation all sensible energy sources are referenced to the datum of saturated water at containment design pressure, which is the maximum amount of energy that can be transferred from the metal to the coolant.

The integrated energy balance at the end of blowdown is presented in the Table 14.3-6. The values were determined by the SATAN Code, as modified by conservative core heat transfer assumptions.

In this calculation the sensible energy sources are transferred to the coolant as a function of time, and for longer blowdown times more sensible energy is absorbed. For the very large breaks, very little energy is transferred to the steam generators, because of the rapid uncovering of the tubes, while for smaller breaks the tubes do not uncover as rapidly and significant heat transfer results. The SATAN Code's model for reverse heat transfer from the steam generator to the Reactor Coolant System is conservatively high, resulting in low or negative values for heat transfer to the steam generator.

A negligible amount of energy is transferred from the reactor vessel during the relatively fast blowdown.

Energy Sinks

Figure 14.3-24 presents the energy absorption capability within the 1.32E+6 ft^3 free volume of the Kewaunee containment. Thus, the internal energy of the steam-air mixture must be increased to 148.4E+6 Btu for the containment pressure to reach the design pressure of 46 psig.

The integrated containment energy balance at the end of the blowdown is given by:

$$U_f = U_i + \Sigma(mh)_{in} + \Sigma Q_{out}$$

where:

U_f	=	Final internal energy in the containment
U_i	=	Initial internal energy in the containment
$\Sigma(mh)_{in}$	=	Enthalpy added by blowdown sources and spray water
ΣQ_{in}	=	Energy added directly to containment atmosphere by hydrogen-oxygen recombination
ΣQ_{out}	=	Heat removal by containment structure and cooling system

The internal energy is made up of three sources: air, steam, and sump water. Only the air-steam mixture with their respective partial pressures contribute to the containment total pressure. The internal energy for the initial assumed containment conditions, 120°F and 15 psia, is as follows:

Steam (m) (u) = (1147) (1051)	= 1.21E+6 Btu
Air (m) (C_v) (T) = (90,542) (0.172) (120)	= 1.86E+6 Btu
Sump (m) (u) = (12,343) (88.0)	= 1.08E+6 Btu
	4.15E+6 Btu

The internal energy balance at the end of blowdown is given in the Table 14.3-7. All entries are in millions of BTUs.

The difference between the internal energies given by the energy balance equation and by the COCO program represents an error of less than +1% the calculation.

Figure 14.3-25 shows the heat transfer coefficient calculated for the various break sizes.

Containment Margin Evaluation

Evaluation of the capability of the reactor containment and containment cooling systems to absorb energy additions without exceeding the containment design pressure requires consideration of two periods of time following a postulated large area rupture of the Reactor Coolant System.

The first period is the blowdown phase. Since blowdown occurs too rapidly for the containment cooling systems to be activated, there must be sufficient energy absorption capability in the free volume of the containment (with due credit for energy absorption in the containment structures) to limit the resulting pressure below design.

Margin - Blowdown Peak to Design Pressure

Point A in Figure 14.3-26 corresponds to the internal energy at the end of the double-ended blowdown, 135.8E+6 Btu. In order for the pressure to increase to design pressure (46 psig) the internal energy must be increased to 148.4E+6 Btu (Point B). The allowed energy addition is therefore 12.6E+6 Btu. Since energy transferred to the containment from the core is in the form of steam, the total transferred core energy corresponding to allowed energy addition is as follows:

$$Q_{core} = \frac{h_{fg}}{h_g} Q_{Allowed} = 12.6E+6 \times \frac{914.8}{1177.8} = 9.8E+6\ Btu$$

This allowable value of energy, which could be transferred from the core to the containment without increasing the transient containment pressure to design pressure, can be compared to the energy stored in the reactor vessel and transferred to the steam generator during blowdown for the double-ended break. The thick metal of the reactor vessel was not

considered since a negligible amount of this energy can be transferred in the short blowdown time.

Stored in the core	9.0E+6 Btu
Core internals Metal	3.3E+6 Btu
Transferred to Steam Generators	-2.9E+6 Btu
	9.4E+6 Btu

Thus, the containment has the capability to limit containment pressure below design even if all of the available energy sources were transferred to the containment at the end of blowdown. This would also include no credit for energy absorption in the steam generator. For this to occur, an extremely high core-to-coolant heat transfer coefficient is necessary. This would result in the core and internals being completely sub-cooled and limit the potential for release of fission products.

Additional Energy Added As Superheat

Line A to C on Figure 14.3-26 represents a constant mass line extended into the superheated region. Comparison of the energy addition allowable for the superheated case relative to the saturated case shows a lesser ability of the containment to absorb an equivalent amount of energy as superheat. An addition of 2.7E+6 Btu of energy after blowdown would cause the containment pressure to increase to design. The recombination of hydrogen and oxygen from a 5.4% Zr-H_2O reaction completed before the end of blowdown would be required to generate 2.7E+6 BTUs of energy. For the case analyzed, the core was assumed to be in a sub-cooled state, and no Zr-H_2O reaction would be possible. In order for Zr-H_2O reaction to occur before the end of blowdown, all of the stored initial energy must remain in the core. If this occurred, a blowdown peak containment pressure of only 37.6 psig would be reached instead of 42.2 psig in the case analyzed. Lines D and E on Figure 14.3-26 represent the superheat energy addition required to increase the pressure to the design pressure and this corresponds to the hydrogen-oxygen recombination energy from a 12.1% Zr-H_2O reaction. It is, therefore, concluded that the containment has the capability to absorb the maximum energy addition from any loss-of-coolant accident without reliance on the containment cooling system. In addition, a substantial margin exists for energy additions from arbitrary energy sources much greater than any possible.

Margin - Post Blowdown Energy Additions

The Safety Injection System is designed to rapidly sub-cool the core and stop the addition of mass and energy to the containment. Thus, it is expected that there will not be any significant energy addition to the containment following blowdown. However, the following cases are presented to demonstrate the capability of the containment to withstand post-accident energy additions without credit for core cooling.

Case 1: Blowdown from a large area rupture with continued addition of the core residual energy and hot metal energy to the containment as steam.

Case 2: Same as Case 1, but with the energy addition from a maximum Zirconium - water reaction.

Figure 14.3-27 presents the containment pressure transient for Case 1. For this case the decay heat generated for a 1721.4 MWt core operated for an infinite time is conservatively assumed. This decay heat is added to the containment in the form of steam by boiling off of water in the reactor vessel. For this case injection water merely serves as a mechanism to transfer the residual energy to the containment as it is produced. Injection water is in effect throttled at the required rate.

In addition, all the stored energy in the core and internals which is calculated to remain at the end of blowdown is added in the same way during the time interval between 12 and 30 seconds (corresponds to accumulator injection time). Also, all the sensible heat of the reactor vessel is added as steam exponentially over 2000 seconds time interval.

The containment cooling system capability assumed in the analysis was one-of-two available containment spray pumps and two-of-four available containment fan coolers. This is the minimum equipment available considering the single failure criterion in the emergency power system, the spray system and the fan cooler system.

The containment heat removal capability starting at 60 seconds exceeds the energy addition rate. Before this heat removal starts, however, the containment pressure exceeds the initial blowdown value, reaching a peak of 44.9 psig. An extended depressurization time results, due to the increased heat load on the containment coolers.

It should be emphasized that this situation is highly unrealistic in that continued addition of steam to the containment at these high rates after blowdown could not occur. The accumulator and Safety Injection System act to rapidly re-flood and sub-cool the core.

Figure 14.3-28 presents the containment pressure transient for Case 2. To realistically account for the energy necessary to cause a metal-water reaction, sufficient energy must be stored in the core. Storing the energy in the core rather than transferring it to the coolant causes a decrease in the blowdown peak.

The reaction was calculated using the parabolic rate equation developed by Baker and assuming that the clad continues to react until zirconium oxide melting temperature of 4800°F is reached. An additional 10% reaction of the unreacted clad is assumed when the oxide melting temperature is reached. A total reaction of 32.3% has occurred by 1000 seconds. Previous analysis has shown that steam limited reactions could result in a higher total reaction but at a much later time. The reaction provided by the parabolic rate equation, therefore, imposes the greatest load on the containment cooling system.

As in Case 1, the residual heat and sensible heat is added to the containment as steam. The energy from the Zr-H_2O reaction is added to the containment as it is produced. The hydrogen was assumed to burn as it entered the containment from the break.

The blowdown peak was reduced to 37.6 psig and a peak pressure of 42.8 psig was reached at 200 seconds. At this time the heat removal capability of the containment cooling system assumed to be operating (one containment spray pump and two fan coolers) exceeded the energy addition from all sources.

For comparison, the containment pressure transients for Cases 1 and 2, and the double-ended blowdown are re-plotted in Figure 14.3-29. It is concluded that operation of the minimum containment cooling system equipment provides the capability of limiting the containment pressure below its design pressure with the addition of all available energy sources and without credit for the cooling effect from the Safety Injection System.

Discussion of Energy Sources Used in Cases 1 and 2

The following is a summary of the energy sources and the containment heat removal capacities used in the containment capability study. Figure 14.3-30 presents the rate of energy addition from core decay heat, Zr-H_2O reaction energy, and the hydrogen-oxygen recombination energy. The heat removal capability for the partial containment cooling (one spray pump and two fan coolers) is also presented. These heat removal values are for operation with the containment at design pressure.

The integrated heat additions and heat removals for Cases 1 and 2 are plotted in Figures 14.3-31 and 14.3-32, respectively. These curves are presented in a manner that demonstrates the capability of the containment and the cooling systems to absorb energy. The integrated heat removal capacity is started at the internal energy corresponding to design pressure, while the integrated heat additions begin from the internal energy calculated at the end of blowdown for each case. The upper line on each curve is the containment structures and containment cooling systems capability to absorb energy additions without exceeding design pressure. The lower curve for each are the energy addition curves, and since these energy additions are the maximum possible with no credit for core cooling, there is more than adequate capability to absorb arbitrary additions.

The curves in Figures 14.3-33 and 14.3-34 present the individual contribution of the heat removal and heat addition source, respectively.

14.3.5 OFF-SITE DOSE CONSEQUENCES

Introduction

The NRC has established guidelines in 10CFR100 for radiation doses resulting from accidental releases of radioactivity from a reactor plant. This section shows the capability of the Kewaunee Nuclear Power Plant to stay within the dose criteria set forth in 10CFR100 following the design basis accident with conservative assumptions including assumed conditions of release consistent with those of NRC Regulatory Guide 1.4.

The Kewaunee Nuclear Power Plant Containment System is described in detail in Section 5. One feature of particular importance to the environmental consequences of a loss-

of-coolant accident is the presence of two barriers, in series, to fission product leakage: the Reactor Containment Vessel and the Shield Building.

Reactor Containment Vessel leakage is collected within an annular volume between these barriers before release; the annulus is, therefore, effective as a means of holding leakage for decay and providing additional dilution prior to release. Release from the Shield Building to the environment is through absolute and charcoal filters provided in the Shield Building Ventilation (SBV) system. For reference in the evaluation of environmental consequences, a schematic diagram of this system is shown in Figure 14.3-35.

Shield Building Ventilation System fans establishes a negative pressure with respect to the atmosphere in the annulus within six minutes after the accident. The amount of filtered annulus air released to the environment is just sufficient to maintain the negative annulus pressure and compensate for in-leakage. The balance of the filtered annulus air is recirculated to the Shield Building to provide for further decay and filtration.

A limited amount of containment leakage could potentially bypass the Shield Building annulus through certain lines that terminate in the Auxiliary Building. This leakage will be collected and processed by the Auxiliary Building Special Ventilation System. An even smaller amount of containment leakage may bypass both the Shield Building and the Auxiliary Building and go directly to the environment. Both of these pathways have been evaluated.

Cause of Activity Release

The postulated cause of radioactivity release to the environment analyzed in this section is an extremely improbable double-ended rupture of a 29-inch inside diameter pipe in the reactor coolant loop. Following the assumptions of NRC Regulatory Guide 1.4, it is assumed that the Design Basis Accident will release to the Reactor Containment Vessel, 100% of the noble gases and 50% of the iodines in the core's fission product inventory to the containment atmosphere. A homogeneous mixture of this activity within the containment atmosphere is assumed to occur instantaneously. Because of the multiple redundancy in engineered safety features, such a release is considered incredible.

Sequence of Events Following a LOCA

As discussed previously, the Shield Building Ventilation System is designed to provide three functions during the course of the loss-of-coolant accident:

- Provide a negative pressure region to control and limit environmental leakage;
- Enhance mixing and dilution of any Containment Vessel leakage to the annulus;
- Provide holdup and long-term filtration of annulus air.

Immediately following the accident, the Shield Building pressure increases due to heat transferred from the containment shell. Operation of one of the Shield Building Ventilation System's two redundant fans establishes a negative pressure within six

minutes. Figure 14.3-37 shows this post-LOCA pressure transient. During this period no credit is taken for the filtered exhaust of air by the Shield Building Ventilation System. Instead it is assumed that the Shield Building does not exist. From 0 to 6 minutes, 90% of the containment leakage is assumed to be released directly to the atmosphere without holdup or filtering. The remaining 10% goes to the Auxiliary Building Special Ventilation Zone where it is filtered before release to the atmosphere.

At 6 minutes into the accident, the Shield Building Ventilation System is assumed to be fully effective in controlling leakage into the Shield Building. It is assumed that from 6 minutes on, 89% of the containment leakage is processed by the Shield Building Ventilation System. Of the remaining 11%, 10% is assumed to go to the Auxiliary Building Special Ventilation Zone where it is subject to processing by the Auxiliary Building Special Ventilation System and 1% is assumed to be released directly to the atmosphere. These leakage assumptions are regarded as conservative upper limits as stated in the Technical Specifications for Kewaunee. A filter efficiency of 90% is applied to the removal of iodine in all forms.

Figure 14.3-36 shows results of a shield building ventilation performance test. Also shown is a curve enveloping all data points with a considerable margin. This envelope is the basis for the conservative exhaust rates used in calculating offsite doses following a LOCA.

As described in Section 5, the shield building ventilation fans take a filtered suction from the annulus and their discharge is apportioned between atmospheric discharge and annulus recirculation flow. The atmospheric discharge (or SBV exhaust flow as shown in Figure 14.3-36) is dependent on the amount of annulus in leakage at the vacuum setpoint chosen for the Shield Building Ventilation System Control. The SBV exhaust flow will reach 2700 cfm at 20 minutes post-LOCA. Using the envelope in Figure 14.3-36, this exhaust flow will maintain a negative 1" wc (water column) in the annulus. It is recognized that exhaust flow rates must be higher early in the accident due to annulus heating. This is shown in Figure 14.3-37A. To simplify the analysis a constant exhaust rate of 5000 cfm has been used from 6 to 20 minutes and 2700 cfm from 20 minutes to 30 days. This simplification can be made due to the size of the envelope in Figures 14.3-36 and 14.3-37A.

The Containment Vessel Internal Spray System is described in Section 6.4. The primary purpose of the spray system is to spray cool water into the containment atmosphere in the event of a LOCA and thereby ensuring that containment pressure does not exceed its design value. However, the spray system also has the property of removing iodine from the containment vessel atmosphere. The iodine removal coefficients due to containment spray are given in Table 14.3-8. Table 14.3-8 also provides a summary of the other parameters used in the analysis.

Method of Analysis

The evaluation of the environmental consequences of a loss-of coolant accident consists of determining the radiation dose to the thyroid resulting from inhalation of radioiodine discharged from the Shield Building and of determining the whole body radiation dose due to

direct gamma radiation from the radioactive cloud created by the discharge of Containment Vessel leakage from the Shield Building.

The evaluation of the environmental consequences of a loss-of-coolant is based on the assumptions of NRC Regulatory Guides 1.4 and 1.109. The analysis model describing the activity release is shown in Figure 14.3-35. This figure illustrates the basic assumptions of the analysis. A portion of the Containment Vessel activity inventory is assumed to leak into the Shield Building and form the Shield Building activity inventory. Activity leaves the Shield Building by passing through the charcoal filters. Of the activity passing through the filters, a portion is released via the shield building vent and the rest is recirculated back into the Shield Building. The Shield Building activity is also a function of the assumed annular participation fraction. This fraction is a measure of the mixing efficiency in the annulus.

The analysis was done using the Bechtel standard computer program LOCADOSE (Reference 1). LOCADOSE calculates radioactivity, integrated activity and releases from up to 19 regions. The regions are separate, but interconnected by mixing flows from one region to another. Calculation of daughter isotopes is also considered. Doses and dose rates can be calculated for each region and for up to 20 off-site locations.

(a) Instantaneous Radioactivity Release

The equation which is used to specify the instantaneous release amount is:

$$A_j^i = AO^i \cdot F_1^i \cdot RF_k \cdot AF_j \qquad (14.3.5\text{-}1)$$

where:

A_j^i is the amount of radioactivity of isotope i in node j in curies

AO^i is the amount of isotope i available for release, in curies

F_1^i is the correction factor for decay of isotope i as explained in equation 14.3.5-2

RF_k is the release fraction of isotope group k, with isotope i belonging to group k

AF_j is the fraction of radioactivity in node j.

The decay correction factor of equation 14.3.5-1 is calculated as follows:

where:

$$F_1^i = e^{-\lambda_D^i (t_1 - t_s)} \qquad (14.3.5\text{-}2)$$

λ_D^i is the decay constant for isotope i, in hrs^{-1}

t_1 is the time of release of the radioactivity, in hrs.

t_s is the time of reactor shutdown, in hrs.

(b) Radioactivity Transfer Terms

Radioactivity is transferred from one region to another by filtered flow and/or unfiltered flow (leakage). The term, which expresses the transfer of radioactivity from region j to region r is:

$$C^i_{jr} = \frac{Q1_{jr} \cdot \left(1 - f^k_{jr}/100\right)}{V_j} + \frac{Q2_{jr}}{V_j} \qquad (14.3.5\text{-}3)$$

where:

C^i_{jr} is the transfer factor from node j to node r for the time interval t_1 to t_2 for isotope i in hrs^{-1}

$Q1_{jr}$ is the filtered flow rate from node j to node r in m^3/hr

f^k_{jr} is the filter efficiency for isotope group k from node j to node r, in percent

V_j is the volume of node j in m^3

$Q2_{jr}$ is the unfiltered flow rate from node j to node r in m^3/hr

(c) Radioactivity Removal Terms

The terms C^i_{jr} represent transfer terms for node j when $j \neq r$.

When j = r this term is a removal term

$$C^i_{jj} = \lambda^{sk}_j - \frac{Q1_{ii} \cdot f^k_{jj}}{100 \cdot V_j} - \sum_{r=1}^{N} \left[\frac{Q1_{ii} + Q2_{jr}}{V_j} \right] - \lambda^i_D \qquad (14.3.5\text{-}4)$$

where:

λ^{sk}_j is the spray removal term in hr^{-1} for isotope group k in node j

$Q1_{jj}$ is the recirculation flow rate for node j in m^3/hr

N is the number of nodes

(d) Matrix Solution

The terms C^i_{jr} discussed in the previous sections about transfer and removal, comprise the coefficients of a set of N differential equations in N unknowns where N is the number of nodes.

Each of these equations has the form for each isotope i:

$$\frac{da^i_j}{dt} = \sum_{\substack{r=1 \\ r \neq j}}^{N} \left[C^i_{rj} \cdot a^i_r\right] - C^i_{jj} \cdot a^i_j + \lambda^i_D \cdot \sum_{m=1}^{P_i} f^{im} \cdot a^m_j \qquad (14.3.5\text{-}5)$$

where:

a^i_j is the activity of isotope i in region j

f^{im} is the fraction of isotope m that decays into isotope i

P_i is the number of parent isotopes for isotope i

The last term of equation 14.3.5-5 accounts for isotopes decaying into a daughter isotope.

(e) Dose Rates at Off-site Locations

Adult inhalation thyroid dose rates at offsite locations is given by:

$$\dot{D}_H(x,t) = BR(x) \cdot \frac{X}{Q}(x) \cdot \sum_{j=2}^{N} \sum_{i=1}^{NISO} \frac{Q1_{j1}\left(1 - f^k_{j1}/100\right) + Q2_{j1}}{V_j} \cdot a^i_j(t) \cdot DCF^i \qquad (14.3.5\text{-}6)$$

where:

$\dot{D}_H(x,t)$ is the inhalation dose rate for location x at time t (rem/hr)

BR(x) is the breathing rate at location x (m^3/sec)

$\frac{X}{Q}(x)$ is the atmospheric dispersion factor (sec/m^3) Q

$Q1_{j1}$ is the filtered flow rate from node j to the atmosphere (m^3/hr)

f^k_{j1} is the filter efficiency from node j to the atmosphere for isotope group k (%)

$Q2_{j1}$ is the unfiltered leakage from node j to the atmosphere

DCF^i is the thyroid dose conversion factor for isotope i

NISO is the number of isotopes

The whole body dose rate is given by:

$$\dot{D}_\gamma(x,t) = FINCOR(x)\cdot\frac{X}{Q}(x)\cdot \sum_{j=2}^{N}\sum_{i=1}^{NISO} \frac{Q1_{ji}\left(1-f^k_{j1}\Big/100+Q2_{j1}\right)}{V_j}\cdot a^i_j(t)\cdot DCF^i_\gamma \qquad (14.3.5\text{-}6)$$

where:

$\dot{D}_\gamma(x,t)$ is the whole body dose rate for location x at time t (rem/hr)

FINCOR(x) is a user specified finite cloud correction factor for location x

DCF^i_γ is the semi-infinite whole body dose conversion factor for isotope i

The calculated whole body dose rate is for the immersion contribution only. It does not include the dose contribution due to shine from the containment.

(f) Doses at Offsite Locations

Adult inhalation thyroid and whole body gamma radiation doses are calculated for each off-site dose point for which a X/Q is supplied.

The inhalation thyroid dose is calculated according to the following expression:

$$D_H(x,\Delta t) = \frac{X}{Q}(x)\cdot BR(x)\cdot \sum_{j=2}^{N}\sum_{i=1}^{NISO} AR^i_j(\Delta t)\cdot DCF^i \qquad (14.3.5\text{-}8)$$

where:

$D_H(x,\Delta t)$ is the inhalation dose at location x for time period Δt (rem)

$AR^i_j(\Delta t)$ is the activity released of isotope i from node j during time period Δt (Ci) $= AR^i_j(\Delta t) = AI^i_j \times C^i_{j1}$

AI^i_j is the integrated activity in region j or the integral of activity over a given time interval Δt

The whole body dose is calculated using:

$$D_\gamma(x,\Delta t) = \frac{X}{Q}(x) \cdot FINCOR(x) \cdot \sum_{j=2}^{N} \sum_{i=1}^{NISO} AR_j^i(\Delta t) \cdot DCF_\gamma^i \qquad (14.3.5\text{-}9)$$

where:

$D_\gamma(x,\Delta t)$ is the whole body dose at location x for time period Δt (rem)

The whole body dose equation is developed Regulatory Guide 1.4, Revision 2. The inhalation thyroid dose equation is developed from Regulatory Guide 1.25.

Dose results are listed in Table 14.3-9 for the base case. The inputs found in Table 14.3-8 represent the base case of LOCADOSE. The two-hour inhalation dose to the thyroid is about 13% of the 10 CFR 100 guideline value and the corresponding whole body dose is about 7%.

Evaluation of Results

Filter efficiency, Shield Building recirculation rate, mixing of leakage within the annular volume, and containment leakrate have been assigned values justified by the containment and safety features design. Since these values are significant to the analysis applied to the plant, studies of the sensitivity of inhalation dose at two hours to variations in these factors have been performed and are discussed in the following text.

Effect of Filter Efficiency

The Shield Building Ventilation System charcoal filters are described in detail in Section 5. Sufficient capacity has been provided to contain all iodine, both inorganic and organic available from leakage (25% of the core inventory) without exceeding safe loadings. The charcoal will be protected from loss of efficiency by moisture separators and heaters, which will insure a relative humidity at or below 70% at rated conditions. Periodic tests of charcoal filter efficiencies have demonstrated iodine removal efficiencies much greater than the 90% assumed for the base case analysis. Therefore it can be expected that these filters will be much more effective in case of an accident than has been assumed for the base case analysis.

Figure 14.3-38 shows the effect of variations in filter efficiency on the two-hour thyroid doses. The curve in Figure 14.3-38 is normalized to the condition of 2300 CFM recirculation flow rate which corresponds to 2700 CFM for SBV exhaust and a negative pressure of 1.0" wc in the Shield Building annulus. The 2-hour thyroid doses at the site boundary are reduced

by 6% if a more realistic 95% filter efficiency is applied to all forms of iodine. If credit were taken for filtering during the first 6 minutes, the reduction would be significantly greater.

Effect of Shield Building Recirculation

With the Shield Building ventilation fan operated at its nominal capacity of 5000 CFM, recirculation flow is the difference between that flow rate and the Shield Building Ventilation (SBV) system exhaust rate. More recirculation flow accompanied by more holdup and more filtration in the Shield Building reduces off-site doses as shown in Figure 14.3-39. The off-site doses under these conditions are given in Table 14.3-9.

Post LOCA offsite dose analyses have typically been performed with the assumption of maintaining a negative 1/4" wc for plants with a secondary containment in accordance with Standard Review Plan 6.2.3. Assuming a negative 1/4" wc differential pressure in lieu of negative 1.0" wc will allow a greater recirculation flow rate that would result in lower off-site doses.

On the other hand, operation of the SBV system without any recirculation flow to the annulus, i.e. operation with a single pass, filtered suction on the annulus with total discharge to the atmosphere, increases the 2-hour thyroid dose at the site boundary by only 6% as shown in Figure 14.3-39. This indicates that the off-site doses will remain well below the 10CFR100 limit if the SBV system operates without any recirculation to the Shield Building annulus.

Effect of Mixing in the Shield Building - (Participation Fraction)

Leakage from the Reactor Containment Vessel disperses and mixes with air in the Shield Building. Mixing is aided by several design features of the Shield Building:

1) During the period immediately following the loss-of-coolant accident, natural circulation flow upward along the Reactor Containment Vessel shell and downward near the Shield Building wall is promoted by the surface-to-ambient temperature differentials.

2) Except for the initial 6-minute period, during which the annulus pressure is assumed positive, all air leaving the annulus is collected by the Shield Building Ventilation System suction header located at the top of the annulus. This location maximizes the distance between the suction header and the most probable sources of leakage, the penetrations, which are all located in approximately the lower third of the Reactor Containment Vessel. Because of the large transport distance from the penetrations to the suction header mixing will be inducted by diffusion and the flow of recirculated air.

3) The filtered air, which is recirculated to the Shield Building is directed by specially designed ducting to sweep past the Containment System penetrations and upward in the annulus with turbulent motion.

Although uniform mixing in the Shield Building is expected, the effect of non-uniform mixing in the Shield Building annulus volume is included in the base case analysis by using a participation fraction of 0.5. This represents the fractional Shield Building volume available for dilution, filtration and recirculation. The effect that variation of the Shield Building participation fraction has on the integrated two-hour thyroid dose is shown in Figure 14.3-40, normalized to a participation fraction of 0.5. In the participation fraction range of 0.50 to 1.00 there is approximately a 3% difference in dose.

Effect of Containment Leak Rate

The Containment Vessel and its penetrations are designed to be leak free and are demonstrated by tests to have a low leakage rate. In fact, periodic tests are performed to show an acceptable margin between tested leakage values and the leak rate of 0.5% per day used in this analysis. Therefore in the event of an accident it can be expected that off-site doses would be significantly less than those which have been calculated.

Three potential pathways have been identified for containment leakage and a fraction of the total leakage has been assigned to each pathway as follows:

- Via the Shield Building, 0.89
- Via the Auxiliary Building, 0.10
- Directly to the environment, 0.01

Assuming these fractions remain the same for different values of total leak rate, then thyroid dose will be proportional to leak rate as shown in Figure 14.3-41.

Leakage rates directly to the environment and via the Auxiliary Building as well as the total integrated leak rate must be demonstrated periodically by test. This could result in a different fractional pattern based on test results and in some cases less reduction in thyroid dose than indicated by Figure 14.3-41. On the other hand a higher fraction of leakage via the Auxiliary Building relative to the Shield Building could result in a greater reduction in thyroid dose because the Auxiliary Building filters are effective immediately whereas there is a time delay before credit is taken for the Shield Building filters.

Conservatism Between Analysis and Physical Situation

Many conservative assumptions have been made in the application of the NRC Regulatory Guide 1.4 to the Kewaunee Plant. Elimination of this conservatism would be expected to reduce the calculated dose by orders of magnitude. In order to place the above analysis in perspective, major assumptions applied in the analysis which affect the calculated dose are reviewed below:

1) In accordance with NRC Regulatory Guide 1.4, a 50% release of iodine and 100% release of noble gases to the containment atmosphere were assumed. As shown in Section 14.3.2, the Safety Injection System will prevent fuel rod clad melting and will limit the zirconium-water reaction to an insignificant amount. However, as a result of the

cladding temperature increase and the rapid system depressurization following the accident, cladding failures may occur in the hotter regions of the core. These failures would release only the inventory of volatile fission products in the gap between the fuel pellet and the cladding.

2) The analysis assumes that the core activity is instantaneously released and homogeneously mixed with the Containment Vessel atmosphere. Since operation of the Safety Injection System will prevent core meltdown, the amount of activity actually released during the first 6 minutes (before a negative pressure in the annulus is established), from the core through the Reactor Containment Vessel and then through the Shield Building annulus to the atmosphere, would be negligible.

3) No reduction of activity has been assumed by plateout in the Shield Building.

4) Recirculation filtration has been assumed to take place with only one of two redundant systems operable. Since the combined flow capability of the two recirculating fans will be double that used in the analysis, a significant reduction of iodine activity in the Shield Building would result.

5) No credit is taken for once through filtering by the Shield Building Ventilation System during the first 6 minutes.

6) The reduction of iodine activity due to containment spray was calculated as part of the KNPP Updated Control Room Habitability Evaluation Report (Reference 2 and 3). This calculation justified an elemental iodine removal rate 'λ' of 27 hr^{-1}. As a conservative estimate of spray removal capabilities, a value of 10 hr^{-1} was used for elemental iodine removal.

14.3.6 DELETED

14.3.7 DELETED

14.3.8 CHARCOAL FILTER IGNITION HAZARD DUE TO IODINE ABSORPTION

The radioactive iodine, which collects on the charcoal filters generates a significant amount of decay heat. A detailed analysis was made of the potential for spontaneous ignition of the charcoal during post-LOCA operation of the Shield Building Ventilation (SBV) system. To maximize the charcoal filter temperature, it was assumed that forced air-cooling is lost at the time of maximum heat load.

Using the assumptions of NRC Safety Guide 4, i.e., 50% halogen release from the fuel and 50% plateout in the Reactor Containment Vessel, the iodine released (from USAR Table 14.3-8b) and the heat generated from that iodine are estimated to be:

Isotope	Curies (10^7)	Decay Heat (kW)
I-131	1.21	41.55

Isotope	Curies (10^7)	Decay Heat (kW)
I-132	1.718	262.7
I-133	2.26	143.7
I-134	2.575	450.3
I-135	2.0	292.7

The maximum amount of heat that can be generated on the filters is limited by the rate at which the iodine leaks out of the Reactor Containment Vessel onto the filters, and by the decay of the isotopes that are collected on the filter.

In the analysis performed, the following conservative assumptions are made:

1) It is assumed that no holdup takes place in the Shield Building, i.e., all of the activity released via Containment Vessel leakage goes directly on the filters.

2) No credit is taken for plateout in the Shield Building.

3) All of the activity is assumed to collect on one train of the SBV filters with 100% efficiency.

With those assumptions, the maximum rate of heat generated on the charcoal filters was predicted to occur at one day following the accident. At this time of maximum heat load, the forced air cooling through the filter assembly is assumed to be lost. Assuming the charcoal filter is at 190°F (based on calculated post-accident shield building temperature) when forced cooling is lost, results in a charcoal filter centerline temperature of 258°F, which is significantly lower than the 626°F ignition temperature of the charcoal used. This temperature is also below the 356°F at which iodine desorption is expected to begin.

This analysis is conservative by at least an order of magnitude for the following reasons:

1) The analysis was based upon a NRC Safety Guide 4 release.

2) The maximum allowable containment vessel leak rate is assumed to occur for the first day following accident initiation. Peak containment pressure occurs only a few minutes following accident initiation and decays quickly. This would result in a leak rate much less than maximum allowable.

3) All plateout of iodine in the Shield Building was neglected.

4) All activity was assumed to collect on one of two filters.

5) All activity leakage from the Containment Vessel was assumed to collect, at 100% filter efficiency, on the filter without holdup or decay in the Shield Building.

6) No heat dissipation from the filter housing to the surrounding room environment is assumed.

A spray system is provided which is activated automatically upon occurrence of high temperature adjacent to the charcoal. The analysis has shown that actuation of this system is not expected to occur.

14.3.9 GENERATION AND DISPOSITION OF HYDROGEN

General

The design basis loss-of-coolant accident and its off-site consequences are discussed earlier in this section. It is recognized that a loss-of-coolant accident could be followed by the possible generation of hydrogen from radiolysis of water, from chemical corrosion of materials by spray liquids, and from possible metal-water reactions accompanying the accident.

The equilibrium concentrations that could theoretically result have been calculated to exceed the lower flammability limit of 4.1 volume percent hydrogen; therefore, it is necessary to provide means of limiting the accumulation of hydrogen to an acceptable lower concentration.

The simplest means of control is to purge, venting the mixture of air and hydrogen to the environment, at a rate sufficient to maintain a hydrogen concentration that is below the lower flammability limit.

The capability of venting is an essential part of any system of hydrogen control because the eventual containment cleanup must be by controlled dispersal of containment gases to the environment; hydrogen control and eventual containment purge by venting are inseparable considerations of the same loss-of-coolant accident because any primary means of control cannot be terminated until conditions permit venting to proceed at a rate sufficient to supplant it and prevent further rise in hydrogen concentration. Complete analysis must be based on a reference condition of acceptable venting at which venting can later proceed at a rate greater than that necessary to control hydrogen. This condition is conveniently defined as the occurrence of 1 MPC at the site boundary (i.e., when the summation of the fractions of the maximum permissible concentrations given in 10 CFR 20 equals unity).

Summary of Reanalysis Based on Safety Guide 7

Initial studies were based on conservative estimates of hydrogen sources provided by the reactor supplier, and they included the assumptions of Safety Guide 4. These studies indicated that 3.5 v/o concentrations would be reached in 56 days, that venting could be deferred until 86 days after the accident without the lower flammability limit being reached, and that initiation of venting through a charcoal filter at a rate sufficient to arrest hydrogen accumulation would then result in instantaneous site boundary concentrations no greater than 1 MPC. Thus, direct venting through charcoal was indicated to be sufficient means of hydrogen control.

The present reanalysis uses a Containment Vessel leak rate of 1% per day. A conservative filter efficiency of 90% for the Shield Building Ventilation System is used in the reanalysis, but doses are also given for a filter efficiency of 95%.

Also, additional considerations are now incorporated in the analysis, based on information published subsequent to initial submittal of this USAR:

1) Safety Guide 7 now prescribes even more conservative assumptions regarding hydrogen sources.

2) Test data indicate that, at least for higher initial post-accident containment temperatures, substantial amounts of hydrogen could be released from painted surfaces during the first day following the accident.

Both the time at which hydrogen control must be initiated and the doses associated with venting are extremely sensitive to the assumptions regarding hydrogen sources. The above two effects, for example, would alone advance the calculated time of occurrence of 3.5% hydrogen concentration -- from 56 days to 18 days and from 56 days to 41 days, respectively. Together they result in occurrence of 3.5% concentration on the eleventh day.

The collective assumptions prescribed in Safety Guide 4 and Safety Guide 7 are regarded as being unnecessarily conservative. Also, for conservatism it is found necessary to overestimate the potential contribution from protective coatings, which is the remaining source, because the test data are for simulated post-accident conditions of temperature much more severe than those predicted. The hydrogen generation and venting problem will consequently be far less severe than determined from these assumptions.

A method of venting is proposed which is indicated to attain reasonable off-site doses, even under these conditions of early venting requirements. In addition, means are provided to defer venting, if necessary, by compressing the containment atmosphere, thus diluting the hydrogen within the containment, and thereby reducing the potential dose from venting.

Methods of Control

Two modes of operation are being provided, any of which employed alone would provide adequate means of hydrogen control.

1) Controlled vent flow and processing of this flow with recirculation filtering by the SBVS before its release to the environment.

2) Deferment of venting, if necessary, by adding air to the containment to compress the atmosphere, and thus, dilute its hydrogen concentration.

In addition to these two methods of hydrogen control, the capability to utilize an external hydrogen recombiner, has been provided.

Venting to the Shield Building Annulus

The Shield Building Ventilation System affords the benefit of recycle through charcoal filters. When venting must first be initiated, at least one-of-two redundant trains of equipment will already be in continuous operation, maintaining vacuum and collecting and processing containment vessel leakage before its discharge to the environment. Any vent flow necessary to maintain acceptable hydrogen concentrations within the containment will be directed into the Shield Building annulus at a controlled rate, to be processed along with the containment leakage which would represent part of the required vent flow.

The effect of recycle through the filters of the Shield Building Ventilation System is to reduce the iodine effluent concentration by an additional factor that is essentially equal to the ratio of recirculation flow to discharge flow to the environment. This is the same factor that has been applied to the standby ventilation system for a boiling water reactor plant (Reference 1).

Analysis demonstrates that venting at greater than 1 MPC would be unnecessary on the basis of reasonably conservative assumptions, which include the conservative allowance for protective coatings. The need to vent at higher activity concentrations might be required only for the extremely conservative basis of Safety Guide 7. If post-accident hydrogen generation were in accordance with this most conservative estimate, the resulting doses from the processed vent flow are indicated to be a small fraction of those of the accident analysis. The time delay before initiation of venting and the conservative allowances made in the initial phases of the accident analysis for direct filtered and unfiltered release of containment leakage are not applicable during the equilibrium recycle operation which will be established before venting is necessary.

Dilution by Containment Pressure Raise

Dilution of hydrogen concentration by modest increases in containment pressure is one of several simpler methods of hydrogen control that were first proposed and investigated in 1970 for Wisconsin Public Service Corporation and other participants of the same study program.

Partial pressurizing of the containment can defer the occurrence of limiting hydrogen concentrations, and consequently defer the need to vent until appreciable decay of containment activity has occurred. The objective is to defer venting until venting at an acceptable dose level alone can arrest the further accumulation of hydrogen and permit termination of the method used to defer venting. Such deferral of venting is the identical objective of other methods, such as the use of recombiners.

Controlled venting with the accompanying replenishment of vented containment air requires that a pressure differential exist at least intermittently across the containment shell. The venting system can readily be designed for an operating differential in either direction, but design for positive internal pressure during venting provides the option of deferred venting.

Analysis indicates that a rather small increase in pressure can significantly reduce the venting doses associated with the most conservative estimates of hydrogen sources, and that the method provides a practical means of utilizing the benefits of recirculation filtration provided by the Shield Building Ventilation System.

Hydrogen Recombination

In addition to the two methods of hydrogen control described above, the capability to use an external hydrogen recombiner for recombination of hydrogen and oxygen into water vapor has been provided. Permanent piping, valving, and power source connections are provided at two separate locations for the connection and operation of an external hydrogen recombiner within the Auxiliary Building. The two locations allow recombiner placement at approximately opposite sides of Containment; in the unlikely situation that one of the two areas will be unavailable or be required for continual personnel occupancy, the remaining location will remain available for recombiner placement and operation.

Analysis of Materials of Construction and Protective Coatings

Analysis has been made of the materials of construction and the protective coatings used within the containment, particularly as they affect the potential of hydrogen generation by reaction with spray solution.

Description of Materials

The original specified coatings for structural steel items were 3 mils of Carbo-zinc 11 primer plus a 4-mil finish coat of Phenoline 305. The same protective coatings were specified for the inner surface of the steel containment vessel, plus an additional 4-mil finish coat between elevations 606 feet and 660 feet. These coatings will be maintained by application of an appropriate Service Level I protective coating or coating system, depending on the extent of repair. Appropriate Service Level I coatings for use in containment are determined by KNPP engineering specifications and procedures. Periodic inspections are performed to assess the condition of protective coatings on the vessel and structural steel.

All concrete walls, floors, ceilings, and other surfaces within the containment were originally protectively coated with sealer, surfacer, and/or finish coats of Carboline or Phenoline. Neither of these coatings included Carbo-zinc. The coatings on concrete walls, floors, ceilings, and other surfaces are also maintained by application of appropriate Service Level I protective coatings. Periodic inspections are performed to assess the condition of protective coatings on concrete surfaces.

The use of unqualified coatings in containment is minimized. Unqualified coatings were used in containment on structural and architectural steel, piping, various equipment and components, and other miscellaneous items. Analysis has shown that the current amount of unqualified paint will not affect the operability of the emergency core cooling system and the internal containment spray system following a loss of coolant accident. Components with

factory coatings, which are unqualified will not be stripped of the coating and re-coated. This is in the interest of equipment reliability and nuclear safety.

Galvanized steel is used for ventilation ducts, gratings, stair treads, etc., and some aluminum is used in components and protective coatings associated with the reactor equipment and the reactor building crane. The use of these materials has been minimized in design to the extent practical.

The surface areas and amount of the materials are summarized in Table 14.3-10.

Other materials in contact with the spray solution, such as stainless steel and copper alloys, are not significant with regard to corrosive generation of hydrogen.

The effects of corrosion on component integrity are of possible concern only with regard to potential chloride stress corrosion of stainless steel by the boric acid spray solution. Sufficient caustic will be added with containment spray so that both the initial spray and the recirculated sump solution will be at pH of 7 or higher. Means are provided to monitor the chloride content of the recirculated sump water. Corrosion effects are not otherwise of concern with regard to component integrity. For example, if the zinc-bearing coating of galvanized ductwork were to be completely consumed by reaction with the spray, there would be negligible further corrosion of the exposed steel.

Zinc-Bearing Surfaces

A zinc-bearing primer is used as an undercoat on original structural steel and on the inner surface of the containment vessel.

The results of ORNL experiments indicate that substantial amounts of hydrogen can evolve from such undercoats during the initial conditions of a loss-of-coolant accident. This effect appears to be independent of the type of spray solution and of the amount and type of coating over the primer. The outer coating is reported to typically appear unaffected after exposure conditions even though measurable releases of hydrogen from the under-coatings were produced.

The most relevant experiments (Reference 2) involved exposure of vendors' test coupons to spray solution under temperature conditions intended to simulate those of a loss-of-coolant accident in a PWR: 5 minutes at 300°F, 105 minutes at 284°F, and the remainder of a day at 225°F. For a boric acid spray solution of 3000 ppm boron without additives, the most applicable paint sample (3 mils of Carbo-zinc plus a 2 mil overcoat) yielded 2.3 cc of hydrogen per cm^2 of surface or 0.075 scf/ft^2. With 0.15N NaOH added to the solution, the yield was 2.0 cc/cm^2 or 0.066 scf/ft^2. The hydrogen release from these tests was typically about 60% of that released in previous tests in which the exposure temperature was maintained constant at 266°F for 24 hours.

The test conditions for both sets of tests were much more severe than those predicted for the design basis accident, and substantially less hydrogen generation would therefore be

expected. It is conservatively assumed in the analysis that the first-day contribution from the painted surface is given by half the product of the total area given in Table 14.3-10 and the release per unit area given by the higher temperature experiments intended to simulate accident conditions.

The factor of two is perhaps justified alone on the basis of the fraction of total zinc-bearing surface that will be directly exposed to the spray solution, but even greater reduction factors should result from the reduced temperatures relative to the experiment. The post-accident air and steam temperatures are predicted to be only 265°F maximum during the first 5 minutes; a decrease from 238°F initial temperature to 140°F during the next 105 minutes; and 140°F or less thereafter.

An approximate indication of the effect of temperature is given by the relation of Arrhenius for the case of constant activation energy: $R(T_1)/R(T_o) = \exp\ [\alpha(T_1-T_o)/T_1T_o]$. Many reactions double or triple in rate for a 10°C rise in temperature in accordance with this relation (Reference 3). Its direct application to the typical relative yields of the ORNL tests (those for the simulated accident conditions vs those for constant temperature exposure at 266°F for 24 hours) implies a doubling in rate for every 4°C rise.

To obtain indication of the reduced reaction that might be expected at the lower temperatures predicted for the design basis accident, the same relation is applied identically to the time-temperature sequence of the accident-simulation experiment and to the predicted first-day post-accident temperature curve. With reaction rates near 300°F assumed to double for temperature increments ranging from 4°C to 30°C, the resulting reduction factors in the first-day reaction are indicated to vary from 109 to 4.2, respectively, relative to the accident simulation tests.

These indications suggest that a reduction factor of 10 to 100 would be appropriate. However, in the absence of lower temperature data or direct indication of temperature sensitivity of the reaction rates, an overall reduction factor of only two is conservatively assumed.

Aluminum Surfaces

Corrosion of aluminum surfaces would be negligible with an acidic borated spray being a factor of a hundred or more less than with a basic spray solution (Reference 4).

For the case of buffered spray solution, reaction rates of aluminum are available as a function of temperature (Reference 5). Application of this information to the temperature transient predicted for the design basis accident indicates 1 mil reaction on the first day, plus a continuing rate thereafter that is less than the 200 mils/year prescribed by Safety Guide 7.

Net Effect of Spray Additive

The total hydrogen production from protective coatings (see Table 14.3-11) has been calculated with and without spray additive, and with first-day production from galvanized

coating treated identically as the paint, because of the absence of relevant information for the case of no additive and because the zinc content of most of the galvanized surface is similar to that of the paint. Addition of spray additive justifies use of a lower conservative estimate for painted surfaces and neglect of the first-day contribution from the galvanized surface.

An added allowance is made for aluminum reaction with additive, based on full consumption of the 110 pounds of aluminum paint on the first day and 200 mils/year consumption of the remaining aluminum, assuming 1/4-inch effective thickness and 20 scf hydrogen generation per pound consumed.

From Table 14.3-11, it can be noted that the calculated total coating contribution at 10 days would not increase with the use of additive. Also, the coating contribution is only a minor part of total continuing production; therefore, the adjustment for additive is not significant. Effectively, the extremely conservative allowance for zinc-bearing surfaces that is necessitated by the absence of lower temperature data, and the appropriate adjustments in this conservative estimate, obscure the net increase in hydrogen that should result from the directly calculable effect of spray additive on aluminum.

Analysis of Methods of Hydrogen Control

Sources and Assumptions

Studies have been based primarily on the results of conservative hydrogen generation calculations provided by the reactor supplier. The major assumptions for this "reasonably conservative case" are summarized in Table 14.3-12, and compared with those for a "most conservative case" which includes the assumptions of Safety Guide 7. Both cases described in this table include the conservative allowance for first-day reaction of the zinc-bearing surfaces described in the previous section.

The significant differences introduced by Safety Guide 7 are the increase in assumed zirconium reaction, the higher value of G, and the greater core gamma absorption in the coolant. These assumptions add a half percent more hydrogen concentration for zirconium and increase the radiolytic sources by factors of 1.67 in the core and 1.6 in the coolant. The overall effect is to alter entirely the urgency and magnitude of the hydrogen problem, as shown by comparison of the first several lines of Table 14.3-13.

The source contributions and venting requirements for the most conservative case that based on Safety Guide 7 are shown in Figure 14.3-43.

Analysis of Venting Through Shield Building Annulus

The first case analyzed is that of controlled venting through the Shield Building Ventilation System without pressurizing, and neglecting any effects of the positive pressure differential that would be used to accomplish such venting.

It is assumed that venting is initiated upon measured occurrence of 3.5 v/o concentration (as shown in Figure 14.3-43), and continued at a diminishing rate which maintains that concentration until the effects of decay and purge depletion result in instantaneous venting concentrations of 1 MPC at the site boundary. The purge and vent rate is then increased with further decay and purge depletion while maintaining 1 MPC off-site, causing containment activity and hydrogen content to decrease monotonically until purging is complete.

Calculations with regard to venting are based on the initial activity inventories described in Appendix D, Table D.1-1 (consistent with Safety Guide 4) and on the dispersion factors and breathing rate appropriate to the two-day to thirty-day period of the meteorological studies:

χ/Q = 3.882E-6 sec/m^3 at the site boundary
= 4.473E-7 sec/m^3 at 4800 meters
ß = 2.32E-4 m^3/sec

Venting is assumed to be through the Shield Building annulus with equilibrium recirculation flow of 4000 cfm through filters which remove 90% of the iodine and all solid fission products, and with constant discharge flow of 200 cfm. These conservatively chosen flow conditions are consistent with the equilibrium phase of the calculations of shield building activity discharge during the design basis accident.

The resulting venting doses are presented in Table 14.3-13.

The reasonably conservative case described in the Table 14.3-13 results in very low off-site venting doses because it represents a rather trivial case of venting. For consistency in comparison of the two cases, it has been assumed that venting is initiated upon occurrence of 3.5% hydrogen. However, the initial activity levels from venting at this time would be about 7 MPC at the site boundary and, by simply deferring venting and allowing the concentration to rise further (to 4.07%), venting could later be initiated at a rate sufficient to arrest the concentration at this higher value without exceeding 1 MPC at the site boundary.

Greater venting doses are indicated in Table 14.3-13 for the most conservative case, but these represent a minor portion of the leakage doses associated with the maximum design accident. They might be regarded as an added penalty from venting, except that occurrence of the postulated accident leakage would have deferred and greatly reduced the consequence of venting (or would have obviated the need to vent, for example, in the case of normal initial leak rate of 1% per day).

Thus, the need to vent at off-site concentrations greater than tolerance in order to maintain safe hydrogen concentrations can be predicted only on the basis of the most conservative assumptions -- those of Safety Guide 7.

For purposes of evaluation of the venting doses, it may be noted that the recycle advantage factor which is incorporated in all the thyroid doses in Table 14.3-13, and which affects the occurrence of 1 MPC vent capability, reduces effectively to nP/L_2 = .90 x 4000 cfm/200 cfm = 18, where P is the recycle flow, L_2 is the discharge flow, and n is the removal

efficiency of the charcoal. This advantage factor is independent of the partition factor or effective volume fraction assumed for mixing in the annulus. The reduction factor would instead be 45 at the expected conditions of 5000-cfm recirculation and 100-cfm discharge, and the iodine doses would then be 40% of those indicated in Table 14.3-13. The thyroid doses would be similarly affected by the removal of iodine by containment spray liquid, which effect, with additive, should further reduce the indicated thyroid doses by a large factor.

Analysis of Effects of Pressure Increase

The lower limit of flammability for hydrogen in air is reported to be 4.1 volume percent at atmospheric pressure, and this limit is reported to increase slightly with pressure, rather than to decrease (Reference 6).

Thus, compression of a hydrogen-air mixture by addition of more air to a fixed volume decreases the volume fraction of hydrogen and permits more hydrogen to be accumulated for a given limiting volume fraction. The calculation assumes that once a limiting concentration C_o is reached (3.5%), as hydrogen production continues, the containment pressure P is raised by injecting air at a rate sufficient to maintain C_o, where a concentration C(t) would otherwise result if dilution by pressure increase did not occur:

$$P(t) = 14.7 \frac{C(t)}{C_o}$$

Doubling of the absolute pressure, for example, would permit hydrogen to be maintained at 3.5% until 7% would otherwise have accumulated.

A second effect is that, when purging and venting are initiated, the fractional vent-flow necessary to maintain a given concentration at any time is less at pressure. The mass flow required is independent of pressure, but the flow expressed in fraction of containment volume is less by the ratio of absolute pressures, as is the fractional release rate of contained activity.

As well as deferring the need to vent, overpressure causes leakage to occur from the containment to the annulus. The assumed leakage is based on 1% per day at the design pressure of 42 psi:

$$L(t) = .01/day \sqrt{\frac{P(t) - 14.7}{42}}$$

Out-leakage and vented gases are necessarily processed identically by the Shield Building Ventilation System. They are consequently equivalent from the standpoint of radiation dose, as well as with regard to their effectiveness in hydrogen control when they are replenished by purge flow. Out-leakage, thus, simply represents a portion of the venting rate that is not deferred when venting is otherwise deferred by pressure increase.

The effects of pressure increase on venting requirements and containment leakage are described by Figures 14.3-44 and 14.3-45, and the resulting doses are presented in Table 14.3-14.

The upper curves of Figure 14.3-44 are for a limited pressure rise of 6.1 psig, sufficient to maintain 3.5% hydrogen up to 30 days. Venting is then initiated at the same rate that compressed air is being added, with pressure consequently remaining constant. This constant pressure purge continues at a rate no more than necessary to maintain constant concentration, until decay and diminishing vent rate cause the concentrations at the site boundary to reduce to 1 MPC. Purging then proceeds at the rate which theoretically maintains 1 MPC and which rapidly increases as a consequence of further decay and purge depletion.

Pressure relief could be initiated between 30 and 62 days in this case, by allowing the vent rate to exceed the purging or replenishment rate necessary to maintain constant hydrogen, but this would unnecessarily increase the venting dose. Similarly, beyond 62 days, pressure relief cannot fully proceed by unreplenished vent relief at the vent rate permitted by off-site concentration limits. Continued existence of hydrogen sources requires instead that some part of the permissible vent flow be replenished as a purging flow to prevent the limiting concentration being exceeded during this period of final cleanup. The solid-line blowdown curves in Figure 14.3-44 are the lines of earliest pressure relief, which maintain both the limiting hydrogen concentration and the limiting off-site radiation concentration during a controlled blowdown plus purge. The time for complete cleanup is independent of pressure or purging considerations; it is determined only by the venting rate and venting depletion of the contained activity, which effect is also idealized in the figures for the case of earliest cleanup.

The resulting doses are shown in Table 14.3-14 and compared with the previous case of no deferment of venting. Deferment to 30 days is seen to gain a reduction of 10 in venting dose, but the containment leakage dose resulting from the overpressure reduces this factor to 5 or 6.

The out-leakage rates are shown in Figure 14.3-45 and compared with the vent rates. The dose from leakage is additive only up to the time when venting is initiated because the purge rates are the total replenished out-flow required to maintain concentration, either by leakage or venting.

Out-leakage should be less than that estimated on the basis of the design leakage, and it will be assured to be less by surveillance testing of the containment. The effects of out leakage are conservatively considered in the calculations but the compensating depletion, which it would cause is neglected because the actual leakage that would occur is uncertain.

The effect of pressure on purge rate may be noted from Figure 14.3-45, where the top solid curve is the fractional purge rate necessary to maintain concentration C_o at atmospheric pressure:

$$L(t) = \frac{Q(t)N}{C_o}$$

with Q(t) the uncompressed volumetric production rate of hydrogen at time t in the containment air volume V that is used to define concentration.

The dashed line describes the initial purge rate at time t that supplants a pressure increase that was maintaining C_o:

$$L(t) = \frac{14.7/P(t)}{C_o} \times Q(t)N = \frac{Q(t)N}{C(t)}$$

where C(t) is the concentration that would occur at time t without dilution by pressure increase.

The solid curve for constant pressure purge initiated at thirty day is:

$$L(t) = \frac{Q(t)N}{C(30)} = \frac{14.7}{P(30)} \times \frac{Q(t)N}{C_o}$$

Thus, required purge rate is reduced with pressure increase directly in the ratio of absolute pressures. Both solid curves in the figure correspond to the same mass flow of air and hydrogen, but they differ with regard to fractional release rate of the contained volume, and hence, with regard to fractional release rate of the contained activity.

The lower curves in Figure 14.3-44 correspond to sustained pressure rise until a 1 MPC venting capability is attained, sufficient to control hydrogen. As shown in Table 14.3-14, the dose is all from out-leakage and little is gained relative to initiating venting at thirty days. However, the containment is tested initially at 46 psig, and thus, its design does not preclude continuing the pressure rise as far as required.

An additional case has been considered wherein recycle credit for iodine is neglected, as would be the case if all leakage and vent flow were somehow to be discharged directly to a 90% filter without recirculation.

The diagonal line in Figure 14.3-45 corresponding to a 1 MPC vent of leak rate would effectively be displaced downward in this case by loss of the recirculation advantage factor of 18.1 for iodine, and the 1 MPC venting intersection would be accordingly deferred in time. It can also be seen from the figure that further pressure rise would cause the containment leakage to exceed the vent rate necessary to control concentration, the intersection occurring at 67 days and 12.8 psi for a 1% leak rate. At this point leakage alone would control hydrogen, the air supply could be reduced to match the leakage and prevent further rise.

A 1 MPC venting capability is deferred in this case to about 90 days, and the total leakage dose is 18.1 rad to the thyroid and 0.107 R whole body radiation. The doses would be less and the maximum useful pressure would be slightly greater for lower containment leak rates.

Thus, even without the recycle advantage at the Shield Building Ventilation System, acceptable doses are indicated for the method of pressure control alone.

Provisions for Mixing, Sampling and Venting of Containment Gases

The provisions for mixing, sampling and venting of containment gases are shown schematically in Figure 14.3-46.

Mixing

Two containment dome vent fans are provided to circulate and mix gases within the containment during the period following the postulated loss-of-coolant accident when combustible gases could conceivably accumulate. Each fan draws a combined 8000-cfm through two-of-four inlet ducts located in the dome area of the Containment Vessel. The discharge from each fan is conveyed downward through separate ductwork and returned to the containment volume near the operating floor.

These systems are completely independent and redundant to each other, and they satisfy the requirements of Engineered Safety Features. The fans are started manually from the control room, and surveillance testing of the capability of these systems to start and operate as intended is performed during refueling outages.

Venting to Shield Building Annulus

Two vent valve systems are provided to accomplish venting of pressurized containment gases to the Shield Building annulus. The two systems will each vent containment gases from the ductwork associated with one of the containment dome vent fans and transfer them by means of positive pressure differential through separate penetrations of the Containment Vessel. Each penetration has remote-manually operated isolation valves that are normally closed and that can be separately opened to permit venting, or sampling, through either penetration. Each penetration exhausts gases through a remotely controlled throttle valve directly into the Shield Building annulus where they will be processed and discharged by the Shield Building Ventilation System.

Each penetration has a remote-indicating flow meter located in the annulus and upstream of the throttle valve so as to indicate fractional containment volume vent rate, independent of containment pressure.

The vent relief systems are located entirely within the annulus to preclude concern for leakage. The systems and their power supplies meet the requirements for engineered safeguards.

The vent system flow requirements are:

- that each throttle valve can accomplish the maximum vent rate necessary to control hydrogen (~25 scfm) at a nominal driving pressure (~2 psi, or greater if necessary); and

- that the combined capacity should not present a significant limitation with regard to time requirements for the completion of containment purge when transfer is eventually made to direct filtered discharge.

Analysis indicates that, for a design leak rate of 1% per day, the resulting out-leakage limits the useful pressure increase to about 13 psi, and this, or an even lower pressure, can be set as an operational limit. The vent system was tested in conjunction with the Shield Building Ventilation System to establish acceptable limits, and limits were set by operating procedures and, if necessary, can be set by means of fixed orifices downstream of the throttle valves.

Compression and replenishment of containment gases is through either of two penetrations that span the annulus to admit fresh air through the instrument air system. These penetrations will each initially be equipped with normally closed, remote manually opened isolation valves, throttle valve, and connections for use of oil-free portable air compressors. Design supply will be 100 scfm for each penetration at the maximum anticipated pressure.

Initial tests of the vent systems included startup, calibration of flow vs control position at varying pressure following integrated leak rate tests, and establishment of limits with regard to the Shield Building Ventilation System.

Provisions for Sampling

Monitoring of the containment hydrogen concentration is accomplished by two Comsip Model K-111 hydrogen analyzers. As stated in reference 7, the analyzers fulfill the requirements of Item II.F.1.6 of NUREG-0737. The hydrogen monitors have indication in the control room and a range of 0% to 10% by volume under positive or negative containment pressure. The monitors are normally kept in standby mode, but indication is available on demand. The system is operated from its remote control panel located outside the high radiation sampling room. A hydrogen sample is drawn from the post-LOCA hydrogen control system sample ports in containment. These ports are located near the discharge of the containment dome fans, which permits rapid detection of hydrogen escaping from the reactor. The fans draw suction from the upper areas of containment, which prevents the formation of a stratified atmosphere. The fans are powered from safeguard buses and are designed to operate in a post-LOCA environment (see NRC SER in reference 7).

14.3.10 STEAM GENERATOR TUBE PLUGGING

Steam generator tube plugs are periodically installed to remove tubes from service based on reported degradation. When installed, the plugs become the primary pressure boundary for the subject tube. Plugs are installed at both ends of the tube, effectively isolating tube wall defects (corrosion, etc.).

A number of plug types and designs have been qualified for use in the KNPP steam generators. Plug types include expanded mechanical plugs (References 1 and 2), rolled plugs (References 3 through 11), and welded plugs (References 12 through 14). Plug integrity is ensured by the qualification of the design and installation process through laboratory testing and observed field performance. Analytical verification of plug integrity used design and operating transient parameters selected to bound those loads imposed during normal and postulated accident conditions. Fatigue and stress analysis of steam generator tube plugs were performed in accordance with the ASME B&PV Code Section III.

All hot leg mechanical plugs identified as potentially susceptible to primary water stress corrosion cracking in NRC Bulletin 89-01 (Reference 15 through 17) were either replaced or repaired using the Westinghouse "Plug in a Plug" (PIP). The PIPs are fastened to the designated plugs by engaging the threads of the plug expander and by tack welding to the plug body. The PIP then limits the amount of primary water, which can pass through the plug if a failure occurred, thus preventing the upper plug section from becoming a projectile, which could penetrate the steam generator tube. All cold leg mechanical plugs potentially susceptible to primary water stress corrosion cracking (Reference 18) were either replaced or repaired using the Framatome "Plug-A-Plug" (PAP) (Reference 20). The PAP has two components, a locking cup and a cap screw. The locking cup is threaded into the head of the ribbed plug forming a seal between the cup and the plug head. The cap screw is then threaded into the mandrel of the plug. The cap screw seals to the locking cup and the locking cup is crimped into flutes on the cap screw head, thereby providing a means of attachment between the upper and lower portions of the ribbed plug. Should the head of the plug sever, the assembly captures the head and prevents a loose part. The PAP assembly also provides a secondary function of limiting the leakage past the ribbed plug.

Tube plugging results in additional flow restriction within the Reactor Coolant System with an associated increase in pressure drop across the steam generators. The current safety analyses (ECCS, etc.) support plant operation with up to 30% of the steam generator tubes plugged (Reference 19).

14.3.11 STEAM GENERATOR TUBE SLEEVING

General

The steam generator tube sleeving design and concept are based on mitigating plugging for tube degradation within the tubesheet crevice and sludge pile (top of tubesheet) areas of the steam generator tube bundle.

A sleeve, when installed, spans the original tube from the tube mouth to a point above the secondary side of the tubesheet. The sleeve is secured at both ends by either a mechanical joint, a welded joint, or a combination of the two. Isolation of tube wall defects (corrosion, etc.) is effected while precluding removal of the tube from service.

A number of sleeve and sleeve joint designs have been licensed for use in the KNPP steam generators, as well as two techniques to repair mechanical sleeve joints that have parent tube degradation in the upper sleeve joint. The first type of sleeve licensed for use was the Westinghouse hybrid expansion joint (HEJ) sleeve. The HEJ sleeves were used for tube repairs during the 1988, 1989 and 1991 refueling outages. The HEJ sleeve, discussed in WCAP-11643 (Reference 1), spans the degraded area of the parent tube in the tubesheet region. The HEJ sleeves are either 36", 30", or 27" to allow access permitted by channel head bowl geometry (References 2 and 3). The sleeve is hydraulically expanded and hard rolled into the parent tubing to form the upper and lower sleeve joints.

During the 1992 and 1996/1997 outage, Combustion Engineering leak-tight welded sleeves were installed. The original sleeve design and installation called for a welded upper and lower sleeve-to-tube joint. This design was later modified to an upper welded joint and a lower hard rolled joint. Topical report CEN-629-P (Reference 4), describes the CE sleeve designs and qualification. Three types of Combustion Engineering leak-tight sleeves are licensed for use. The first type, the straight tubesheet sleeve, spans the degraded area of the parent tube in the tubesheet crevice region. The sleeve is welded to the parent tube near each end. The second type of sleeve is a full depth tubesheet sleeve, which is welded near the sleeve upper end and hard rolled into the tube and tubesheet at the sleeve lower end. A variation on the tubesheet sleeve design is the use of a pre-curved sleeve, which allows access to the outer periphery of the tube bundle. The third type of sleeve, the tube support plate sleeve, spans the degraded area of the tube support plate and is installed up to the sixth support plate. This sleeve is welded to the parent tube near each end of the sleeve.

In 1996, the Westinghouse laser welded sleeve was licensed for use in accordance with WCAP-13088 (Reference 5). There are two types of laser welded sleeves that can be installed, tube support plate sleeves and tubesheet sleeves. The tube support plate sleeve is 12" long and spans the degraded area of the tube adjacent to the support plate intersection. The tube support plate sleeve is hydraulically expanded and laser welded at each end. The pressure boundary portion of the tube support plate sleeve is the weld and the sleeve section between the welds. Tubesheet sleeves extend from the tube end to above the top of the tubesheet. Standard and bowed, or peripheral, tubesheet sleeves can be installed. The upper or free span joint is hydraulically expanded and laser welded. The lower joint is hydraulically expanded and roll expanded. Standard tubesheet sleeves extend from 27" to 36" in length, while bowed tubesheet sleeves extend from 30" to 36" in length. The pressure boundary portion of the tubesheet sleeve is the weld and below down to the tubesheet primary face.

During the 1994 and later tube inspections, parent tube indications (PTI) were detected in the lower hardroll transition of the upper HEJ. The majority of degradation was found to be occurring within the parent tube and sleeve pressure boundary of the upper HEJ. An attempt was made to redefine the pressure boundary using the diametrical difference criterion. The criterion was not as successful as had hoped, and therefore, a laser welding process was licensed to repair the upper HEJ to allow the sleeved tube to remain in service. The laser welded repair and process qualification work are discussed in WCAP-14685, Revision 3, (Reference 7) and WCAP-14685, Revision 2, Addendum 1 (Reference 8).

In the process of performing the laser-welded repair a number of difficulties were encountered. As a result a process was developed and licensed to re-sleeve the HEJ sleeved tubes. The process, described in topical report CEN-632-P (Reference 9), involves removing the lower portion of the existing sleeve, expanding the upper remaining HEJ and installing a longer replacement sleeve. The sleeve design used is a Combustion Engineering leak-tight sleeve that is up to 39 inches in length.

In 1998, the pressure boundary of the upper HEJ sleeve joint was again redefined to allow PTIs detected in the lower hard roll transition of the upper joint to remain in service. The revised pressure boundary allows PTIs located a minimum of 0.92 inch (plus an allowance for NDE uncertainty) below the bottom of the hard roll upper transition to remain in service. This distance is measured on the inside of the HEJ sleeve. The basis for the upper HEJ pressure boundary criterion is documented in WCAP-15050 (Reference 6). Technical Specification Amendments 139 and 146 approved application of this pressure boundary definition for operating cycles 23 and 24 (References 11 and 13).

For all sleeve designs, sleeve/tube structural integrity is ensured by the qualification of the design and installation process through laboratory testing and observed field performance. Analytical verification of sleeve/tube integrity used design and operating transient parameters selected to bound those loads imposed during normal, upset accident and operating conditions. Fatigue and stress analysis of sleeved tube assemblies were performed in accordance with ASME B&PV Code Section III. Sleeve assemblies are monitored through periodic inspection, and sleeve examinations are performed using qualified techniques and personnel (Reference 10).

Tube sleeving results in additional flow restriction within the reactor coolant system with an associated increase in pressure drop across the steam generators. This flow restriction is calculated based on identified hydraulic equivalency ratios: the number of installed sleeves, which result in a flow reduction equivalent to one plugged tube. The current safety analyses (ECCS, etc.) support plant operation with up to 30% of the steam generator tubes plugged (Reference 12).

Conclusion

On the basis of a qualified design and installation process, and verification by analytical methods and laboratory testing, steam generator tube sleeving provides a primary system boundary equivalent to that of the original steam generator tube (References 1 through 9).

Any combination of tube sleeving and plugging up to the allowable 30% equivalent tube-plugging limit is bounded by the analyses for 30% tube plugging.

The NRC has reviewed and accepted the tube sleeving methodology in NRC Safety Evaluation Reports (SERs) approving Technical Specification Amendments No. 76, 127, 128, 134, 135, and 139.

14.3.12 STEAM GENERATOR TUBE FATIGUE ANALYSIS

The NRC issued Bulletin 88-02 (Reference 1) which required several actions to be implemented in order to minimize the potential for a steam generator tube rupture event caused by a rapidly propagating fatigue crack. Analysis results concluded that no modification, preventive tube plugging, or other measure is necessary at Kewaunee to preclude a fatigue rupture similar to North Anna Unit 1 event on July 15, 1987 (Reference 2). The NRC has accepted the analysis results (References 3 and 4). Significant changes to steam generator operating parameters (e.g., steam pressure and flow) relative to the reference parameters assumed in the current analysis are monitored during power escalation to ensure the current operating parameters are within acceptable limits to preclude steam generator tube rupture event caused by a rapidly propagating fatigue crack (Reference 5).

14.3.13 VOLTAGE BASED REPAIR CRITERIA FOR STEAM GENERATOR TUBES

Technical Specification Amendment No. 126 approved application of a voltage-based repair criteria for steam generators tubes experiencing outside diameter stress corrosion cracking at the tube support plate intersections (Reference 1). To support implementation of the voltage-based repair criteria an evaluation was performed to determine the maximum permissible steam generator primary-to-secondary leak rate during a steam line break event (Reference 2). The evaluation considered both a pre-accident and accident initiated iodine spike for off-site dose and control room operator dose. The results of the evaluation show that the control room operator dose with an accident initiated spike yields the limiting leak rate. This case was based on a 30 rem thyroid dose and initial primary and secondary coolant iodine activity levels of 0.20 μCi/gm (Reference 3) and 0.10 μCi/gm dose equivalent I-131 respectively. A leak rate of 3.69 gpm was determined to be the upper limit for the faulted loop while the intact loop was assumed to leak at 0.1 gpm (150 gpd per TS 3.1.d.2). Thirty rem was selected based on guidance of the Standard Review Plan. The whole-body doses due to noble gas immersion have been determined to be less limiting than the corresponding thyroid doses.

14.3.14 F* AND ELEVATED F* ALTERNATE REPAIR CRITERIA FOR STEAM GENERATOR TUBES

Technical Specification Amendment No. 138 approved application of the F* and EF* alternate repair criteria for indications of degradation occurring within the tubesheet crevice region (Reference 1). Tubes with indications of degradation in either the original factory roll expansion, or the unexpanded portion of the tube within the tubesheet, may be dispositioned for continued service or repaired though application of the F* or EF* criteria. The F* and EF* criteria are described in WCAP-14677, Revision 1 (Reference 2). The F* and EF* criteria were established using guidance consistent with RG 1.121. Neither the F* or EF* criteria will significantly contribute to off-site dose following a postulated main steam line break such that contributions from these sources need to be included in offsite dose analyses. Inherent to these criteria is the ability to perform an additional roll expansion of the tubes, either as an extension of the original factory roll, in which case the F* criteria

applies, or in the area starting approximately 4" below the top of the tubesheet, in which case the EF* criterion applies. The additional roll expansion procedure (References 3 and 4) can be applied over existing degradation provided the F* or EF* requirements for non-degraded roll expansion lengths of 1.11" (plus an allowance for NDE uncertainty) and 1.51" (plus an allowance for NDE uncertainty), respectively are satisfied (Reference 2). The NDE uncertainty applied to the F* and EF* distance is a function of the eddy current probe and technique used. Any indications of degradation existing below the F* and EF* distance is acceptable for continued service.

14.3.15 STEAM GENERATOR TUBE REMOVAL

Portions of steam generator tubes are removed periodically for laboratory analysis to determine degradation morphology, extent, and cause. Upon removal, the affected tube portions remaining inside the steam generator are plugged on both ends to maintain the integrity of the pressure boundary. Analyses have been performed to define the acceptable locations for removing tubes (Reference 1). The plugs installed to restore pressure boundary integrity are qualified to the requirements of ASME B&PV Code Section III. Table 14.3-15 indicates the location of tubes removed.

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1. WCAP-11643, "Kewaunee Steam Generator Sleeving Report", Revision 1, November 1988 (proprietary)

2. Letter from G. Goldberg (Westinghouse) to S. Bernhoft (WPS), "Structural Analysis of the Kewaunee Series 51 Steam Generator 27" Tubesheet Mechanical Sleeve", dated January 7, 1991

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8. WCAP-14685, Revision 2, Addendum 1, "Laser Welded Repair of Hybrid Expansion Joint Sleeves for Kewaunee Nuclear Power Plant Addendum 1: Evaluation of Weld Repaired HEJ Sleeved Tubes", April 1997

9. CEN-632-P, Revision 0, "Repair of Kewaunee Steam Generator Tubes Using a Resleeving Technique", April 1997 (proprietary)

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2. WCAP-12711, "Re-Evaluation of U-Bend Tube Fatigue For The Kewaunee Plant Steam Generators", September 1990 (proprietary).

3. Letter from J.G. Giitter (NRC) to C.R. Steinhardt (WPSC) transmitting the NRC SER for closeout of Bulletin 88-02, Letter No. K-89-59 dated March 22, 1989.

4. Letter from A.G. Hansen (NRC) to C.A. Schrock (WPSC) transmitting the NRC Concurrence of the Westinghouse reassessment of Bulletin 88-02, Letter No. K-92-227 dated December 8, 1992.

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2. Letter from S.P. Swigart (Westinghouse) to R.P. Pulec (WPSC), "Kewaunee Steam Line Break Off-Site and Control Room Doses", Letter No. WPS-98-035 dated August 26, 1998

3. Letter from W.O. Long (NRC) to M.L. Marchi (WPSC) transmitting the NRC SER for Amendment No. 140 to the Operating License, reduction of Reactor Coolant System specific activity, Letter No. K-98-143 dated October 27, 1998

REFERENCES - SECTION 14.3.14

1. Letter from W.O. Long (NRC) to M.L. Marchi (WPSC), transmitting the NRC SER for Amendment No. 138 to the Operating License, Application for F* and EF* Alternate Repair Criteria For Steam Generator Tubes, Letter No. K-98-129 dated September 22, 1998

2. WCAP-14677, Revision 1, "F* and Elevated F* Tube Alternate Repair for Tubes With Degradation Within the Tubesheet Region of the Kewaunee Steam Generators", May 1998 (proprietary)

3. WCAP-14679, Qualification of Additional Roll Expansion for the Kewaunee Nuclear Power Plant Steam Generators, June 1996 (proprietary)

4. 51-5002057-00, Re-Roll Qualification Report for Kewaunee Steam Generators (proprietary)

REFERENCES - SECTION 14.3.15

1. STD-7.2.4-7006, Generic Cut Tube Evaluation For Model D4, Series 44, and Series 51 Steam Generators, January 1987 (proprietary)

TABLE 14.0-1
INSTRUMENTATION DRIFT AND CALORIMETRIC ERRORS
NUCLEAR OVERPOWER TRIP CHANNEL

	Set Point and Error Allowances: (% of rated power)	Estimated Instrument Errors: (% of rated power)
Nominal set point	109	-
Calorimetric error	2	1.55
Axial power distribution effects on total ion chamber current	5	3
Instrumentation channel drift and set point reproducibility	2	1.0
Maximum overpower trip point assuming all individual errors are simultaneously in the most adverse direction	118	-

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TABLE 14.3-1
SMALL BREAK LOCA
TIME SEQUENCE OF EVENTS

Event	2 in. (sec)	3 in. (sec)	4 in. (sec)	6 in. (sec)
Start	0.0	0.0	0.0	0.0
Reactor Trip	25.95	11.53	7.12	4.97
S-Signal	30.30	12.64	7.86	5.53
AFW Initiation	630.0	630.0	630.0	630.0
Loop Seal Venting	682.0	303.5	170.7	37.7
Top of Core Uncovered	N/A	387.0	158.1	N/A
Accumulator Injection	1918.0	693.6	348.4	149.8
Maximum Core Uncovery	N/A	707.6	165.6	N/A
Peak Clad Temperature Occurs	N/A	752.6	249.56	N/A
Top of Core Covered	N/A	900.0	411.1	N/A

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TABLE 14.3-1a
SMALL BREAK LOCA
FUEL CLADDING RESULTS

Results	2 Inch	3 Inch	4 Inch	6 Inch
Peak clad temperature (NF)	N/A	1054[1]	871	N/A
Peak clad temperature location (ft)	N/A	10.75	10.0	N/A
Local Zr/H_2O reactor, maximum (%)	N/A	0.0354	0.0337	N/A
Local Zr/H_2O location (ft)	N/A	11	10	N/A
Total Zr/H_2O reaction (%)	N/A	< 1.0	< 1.0	N/A
Hot rod burst time (sec)	N/A	N/A	N/A	N/A
Hot rod burst location (ft)	N/A	N/A	N/A	N/A
NSSS Power, 102% of	1683 mWt			
Peak Linear Power, 102%of	12.36 kW/ft			
Peaking Factor (At license Rating)	2.50			

[1] The temperature in the WCAP-14103 (Reference 6) analysis was 1053°F. Various credits and penalties identified in letter WPS-00-019 (Reference 7), result in an adjusted PCT of 1054°F. This value does not include the Reference 7 note (a) benefit of 109°F, since cycle 24 contains some initially once burnt Siemens 14x14 standard fuel.

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TABLE 14.3.2-1
INITIAL CONDITIONS FOR THE KEWAUNEE NUCLEAR PLANT
WCOBRA/TRAC LARGE BREAK LOCA ANALYSIS

Parameter	Analysis Value
Plant Internals	Flat Upper Support Plate
Barrel Baffle Design	Downflow
Core Bypass Flow (%)	6.92
Licensed Core Power (MWt)	102% of 1650
System Pressure with Uncertainties (psia)	2280.0
Primary System Fluid Temperatures: T_{HOT} (°F) T_{AVG} (°F) T_{COLD} (°F)	 596.69 562.00 527.31
Fuel Type	Siemens 14x14 Heavy Fuel
Fuel Data Source	PAD 3.4 [Ref 9]
Rod Backfill Pressure (psig)	365.0
Total Peaking Factor, F_Q	2.350
Nuclear Enthalpy Rise Peaking Factor, $F_{\Delta H}$	1.700
Peak Linear Power (kW/ft): Appendix K Superbounded	 14.661 14.661
Relative Power in the Outer Core Channel	0.45 (evaluated to 0.60)
Loop Volumetric Flow Rate (gpm) Loop Mass Flow Rate (lb.m/sec)	83,400 8969
Reactor Coolant Pumps	Running
Steam Generator Tube Plugging (%)	30.0
Steam Generator Secondary Pressure (psia)	642.32
Accumulators in Operation	2 (one injects into the Intact Loop; one spills to containment)
Accumulator Conditions per Accumulator: Water Volume (ft³) Nitrogen Pressure (psia) Water Temperature (°F)	1250.0 714.7 90.0 in Appendix K ; 108.0 in Superbounded

TABLE 14.3.2-1 INITIAL CONDITIONS FOR THE KEWAUNEE NUCLEAR PLANT WCOBRA/TRAC LARGE BREAK LOCA ANALYSIS	
Parameter	**Analysis Value**
Number of Safety Injection Pumps in Operation	1 LHSI injecting into the Upper Plenum; 1 HHSI injecting, spilling to containment pressure; 1 LHSI and 1 HHSI pump assumed to fail
Safety Injection Conditions: Pump Flow Water Temperature (°F) HHSI Delay Time (sec) LHSI Delay Time (sec)	 Degraded 10% for HHSI; 5% for LHSI 80.0 20.0 after S-signal (offsite power available) 25.0 after S-signal (offsite power available)
Containment Pressure	See Figure 14.3.2-2

TABLE 14.3.2-2 ASSUMPTIONS USED IN THE APPENDIX K CALCULATION	
PLANT CONFIGURATION	**VALUE**
Pressurizer Location	Intact Loop
Total Peaking Factor, F_Q	2.350
Nuclear Enthalpy Rise Peaking Factor, $F_{\Delta H}$	1.700
Core Power (MWt)	102% of 1650.0
Loop Flow Rate	Thermal Design Minimum Flow
Hot Assembly Burnup	Beginning of Cycle
MODELING ASSUMPTIONS	**VALUE**
ECCS Worst Single Failure	Loss of a LHSI Pump
ECCS Spilling Assumption	One HHSI Pump injecting, spilling to containment pressure
Accumulator Nitrogen	Modeled
Reactor Coolant Pump Model	Conservative Two-Phase Model
Pump Rotor	Not Locked During Re-flood
Cross-Flow De-Entrainment	Modeled
Limiting Break Discharge Coefficient, C_D	0.4 [References 3 and 4]
Containment Pressure	Lower Bound
Decay Heat Model	ANS 1971 Decay Heat + 20%
Metal-Water Reaction Model	Baker-Just
Swelling and Blockage	Modeled
Clad Burst	Modeled
Nucleate Boiling During Blowdown	Not Allowed
ECCS Bypass Model	Conservative

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TABLE 14.3.2-3 ASSUMPTIONS USED IN THE SUPERBOUNDED CALCULATION	
PLANT CONFIGURATION	**VALUE**
Pressurizer Location	Intact Loop
Total Peaking Factor, F_Q	2.350 (equiv. to Appendix K F_Q of 2.585)
Nuclear Enthalpy Rise Peaking Factor, F_{DH}	1.700
Core Power (MWt)	102% of 1650.0
Loop Flow Rate	Thermal Design Minimum Flow
Hot Assembly Burnup	Beginning of Cycle
MODELING ASSUMPTIONS	**VALUE**
ECCS Worst Single Failure	Loss of a LHSI Pump
ECCS Spilling Assumption	One HHSI Pump injecting, spilling to containment pressure
Accumulator Nitrogen	Not Modeled
Reactor Coolant Pump Model	Conservative Two-Phase Model
Pump Rotor	Locked During Re-flood
Cross-Flow De-Entrainment	Not Modeled
Limiting Break Discharge Coefficient, C_D	0.6 [References 3 and 4]
Containment Pressure	Lower Bound
Decay Heat Model	1979 ANS Decay Heat with 2-σ Unc'y
Metal-Water Reaction Model	Cathcart-Pawel with 2-σ Unc'y
Swelling and Blockage	Not Modeled
Clad Burst	Not Modeled
Nucleate Boiling During Blowdown	Allowed
ECCS Bypass Model	Realistic

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TABLE 14.3.2-4
KEWAUNEE LARGE BREAK LOCA CONTAINMENT DATA
(Note that this data is used for minimizing the containment pressure)

Plant Configuration	**Analysis Value**
Net Free Volume (ft^3)	1.37×10^6
Initial Conditions:	
Pressure (psia)	14.7
Temperature (°F)	90.0
RWST Temperature (°F)	45.0
Service Water Temperature (°F)	32.0
Containment Exterior Shell Temperature (°F)	-20.0
Spray System:	
Number of Pumps Operating	2
Runout Flow Rate, each (gpm)	1,600
Fastest Post-LOCA Initiation of Spray System (sec)	15.0
Safeguards Fan Coolers:	
Number of Fan Coolers Operating	4
Fastest Post-LOCA Initiation of Fan Coolers (sec)	0.0
Fan Cooler Performance	See Table 14.3.2-5
Structural Heat Sink Data	See Table 14.3.2-6

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TABLE 14.3.2-5
KEWAUNEE LARGE BREAK LOCA FAN COOLER PERFORMANCE DATA
(Per Fan Cooler)

Steam / Air Temperature (°F)	Heat Removal (Btu/sec)
120.0	1,800
136.0	5,670
205.0	12,550
244.0	16,970
270.0	20,080

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TABLE 14.3.2-6 KEWAUNEE LARGE BREAK LOCA STRUCTURAL HEAT SINK DATA			
Heat Sink	**Material Type of Each Layer**	**Thickness (ft) of Each Layer**	**Total Exposed Area (ft^2)**
1.5 in steel	Steel Paint (7 mils)	0.125 0.000583	26,381
3/4 in steel	Steel Paint (11 mils)	0.0625 0.000917	17,318
1/4 in steel / 1 in concrete	Steel Concrete Concrete	0.02083 0.5 0.5	1,260
1/4 in steel / 12 in concrete	Stainless Steel Concrete Concrete	0.02083 0.5 0.5	6,600
1.5 in steel	Steel Paint (11 mils)	0.125 0.000917	17,823
3/4 in steel	Steel Paint (3 mils)	0.0625 0.000250	9,877
3/8 in steel	Steel	0.03125	6,800
1/2 in steel	Steel	0.04167	44,000
3/4 in steel	Steel	0.0625	4,823
1/4 in steel	Steel	0.02083	32,000
3/16 in steel	Steel	0.01563	35,125
0.090 in steel	Steel	0.0075	12,400
0.144 in steel	Steel	0.012	1,695
0.100 in steel	Steel	0.00833	6,000
1.440 in steel	Steel	0.12	2,200
12 in concrete	Concrete Concrete	0.5 0.5	3,400
12 in concrete	Concrete Concrete Paint (8 mils)	0.5 0.5 0.000667	37,400
6 in concrete	Concrete	0.5	19,700
6 in concrete	Concrete Paint (8 mils)	0.5 0.000667	5,300
3 in concrete	Concrete	0.25	2,370
3 in concrete	Concrete Paint (8 mils)	0.25 0.000667	5,200

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TABLE 14.3.2-7 KEWAUNEE LARGE BREAK LOCA MASS AND ENERGY RELEASES		
Time (sec)	**Mass Release (lbm/sec)**	**Energy Release (Btu/sec)**
0.0	0.	0.
0.5	45700.	23700000.
2.5	30000.	15800000.
7.0	11000.	7800000.
10.0	6000.	4400000.
12.5	7300.	4000000.
20.0	3000.	1000000.
22.0	6200.	1570000.
27.0	0.	20000.
33.0	0.	50000.
35.0	4100.	750000.
40.0	700.	340000.
50.0	100.	100000.
85.0	100.	90000.
135.0	800.	240000.
175.0	200.	115000.
225.0	150.	90000.
275.0	250.	125000.
300.0	250.	125000.

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TABLE 14.3.2-8 KEWAUNEE LARGE BREAK LOCA SEQUENCE OF EVENTS		
	Transient Time (sec)	
Event	Appendix K	Superbounded
Accident Initiation	0.00	0.00
Reactor Trip Signal	6.40	6.32
Safety Injection Signal	6.93	6.84
Accumulator Injection Begins	7.50	6.50
Blowdown PCT Occurs	7.50	7.00
End of ECCS Bypass	20.58	
End of Blowdown	20.58	17.23
Hot Rod Burst Occurs	25.67	-
High Head Safety Injection Begins	26.93	26.84
Bottom of Core Recovery	30.17	30.00
Low Head Safety Injection Begins	31.93	31.84
Accumulator Water Empty	33.30	31.45
Hot Assembly Burst Occurs	40.56	-
Re-flood PCT Occurs	185.0	87.50

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TABLE 14.3.2-9 KEWAUNEE LARGE BREAK LOCA RESULTS		
	Transient	
Event	Appendix K	Superbounded
Calculated Blowdown PCT (°F)	1670	1823[2]
Blowdown PCT Location (ft)	7.625	8.125
Calculated Re-flood PCT (°F)	2001[1]	1812[2]
Re-flood PCT Location (ft)	8.125	6.875
Maximum Local Zr / H_2O Reaction	3.3 %	< 17.0 %
Local Zr / H_2O Location (ft)	7.875	6.875
Total Corewide Zr / H_2O Reaction	0.0033%	< 1.0 %

1 The temperature in the letter WPS-98-020 (Reference 10) analysis was 1872°F. Various credits and penalties identified in letter WPS-00-005 (Reference 11) result in an adjusted PCT of 2001°F.

2 In the Superbounded calculation, peak clad temperatures at the 95th percentile probability level with 95% confidence is obtained by adding the calculated peak clad temperature to the code bias plus uncertainties. The Superbounded peak clad temperatures listed in the above table already include the uncertainties. The sum of the code bias plus uncertainties has been determined to be 260.5°F in blowdown and 175.0°F in reflood (References 3 and 4).

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TABLE 14.3-3 COMPONENTS NOMENCLATURE	
Component	**Element**
Vessel Supports	Numbers 1 and 49
Barrel Flange and Hold-Down Spring	Numbers 2 through 6
Barrel	Numbers 7 through 10
Lower Core Supports	Numbers 11 through 15
Major Fuel Assembly	Even Numbers 16 through 38
Minor Fuel Assemblies	Odd Numbers 17 through 39
Upper Internals	Numbers 40 through 48

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TABLE 14.3-3A
MAXIMUM DEFLECTIONS UNDER BLOWDOWN (INCHES)
(1-MILLISECOND DOUBLE-ENDED BREAK)

Component	Cold Leg	Hot Leg	Seismic Horizontal	Maximum Total	Allowable Limit	No Loss of Function Limit
Upper Barrel - radial inward - radial outward	 0.0 0.093	 0.064 0.021	 0.002 0.002	 0.066 0.095	 2.0 0.5	 4.0 1.0
Upper Core Plate	0.004	0.014	0	0.014	0.100*	0.150
RCC Guide Tubes (deflection as a beam)		31 < Allowable	0.006 (ALL)	31 < Allowable	0.72 (ALL)	1.15(ALL)
Total (33)		2 < N.L.F.	0.006 (ALL)	2 < N.L.F.	0.72 (ALL)	1.15(ALL)
Fuel Assembly Thimbles (cross section distortion)	~ 0	~ 0	~ 0	~ 0	0.036	0.072

* Only to verify that the plate will not touch a guide tube.

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TABLE 14.3-3B
SUMMARY OF MAXIMUM STRESS INTENSITIES (PSI)
(1-MILLISECOND PIPE BREAK AND SEISMICS)

	Hot Leg Break		Cold Leg Break		Maximum Total Seismic		Maximum Total Blowdown Plus Seismic
Component	**Maximum Membrane**	**Maximum Total**	**Maximum Membrane**	**Maximum Total**	**Vertical**	**Horizontal**	
Barrel (Girth Weld)	10,430	20,199	32,000	45,300	718	2,170	48,188
Barrel Flange (Weld)	7,778	29,568	25,000	44,500	313	1,007	45,820
Fuel Assembly Top Nozzle Plate	--	10,380	0	850	0	0	10,380
Fuel Assembly Bottom Nozzle Plate	0	44,060	0	25,080	0	0	44,060
Upper Support Columns	10,228	10,228	14,043	14,043	--	--	14,043

Allowable Stress, Sm:
Sm at 588°F = 16,600 psi*
maximum membrane stress = P_m = 2.4 Sm = 39,800 psi
maximum total stress = $P_m + P_b$ = 49,800 psi

* Per Winter 1969 Addenda ASME Section III Code.

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TABLE 14.3-4
STRUCTURAL HEAT SINKS

Heat Sink	Material	Area, ft^2	Thickness, in.	Density, lb/ft^3	Heat Capacity, Btu/lb°F	Conductivity Btu/hr ft°F
Containment Dome	Steel liner	17,300	0.75	490	0.115	26
	Concrete		12	144	0.2	0.8
Containment Cylinder	Steel liner	39,000	1.5	490	0.115	26
	Concrete		12	144	0.2	0.8
	Concrete	11,730	12	144	0.2	0.8
	Concrete	17,320	12	144	0.2	0.8
	Concrete	1,120	12	144	0.2	0.8
	Steel liner	1,140	1.5	490	0.115	26
	Concrete		12	144	0.2	0.8
	Steel liner	5,100	1.5	490	0.115	26
	Concrete		12	144	0.2	0.8
	Steel	4,055	0.336	490	0.115	26
	Steel	6,925	0.5	490	0.115	26

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TABLE 14.3-5 ENERGY SOURCES		
1. Reactor Coolant System Internal Energy		163.02E+6 Btu
2. Accumulator Internal Energy (one)		6.38E+6
3. Initial Core Stored Energy		19.11E+6
4. Core Internals Metal Energy		6.51E+6
5. Reactor Vessel Metal (below vessel nozzle)		5.91E+6
Subtotal		200.93E+6
6. Core Power Generation During Blowdown		
a. Double Ended		3.04E+6
b. 4.5 ft^2		3.63E+6
c. 3.0 ft^2		4.30E+6
d. 0.5 ft^2		10.53E+6
7. Zr-H_2O Reaction		~ 0
TOTALS	a. Double Ended	203.97E+6
	b. 4.5 ft^2	204.56E+6
	c. 3.0 ft^2	205.23E+6
	d. 0.5 ft^2	211.46E+6

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TABLE 14.3-6 INTEGRATED ENERGY BALANCE*				
	DE	**4.5 ft²**	**3.0 ft²**	**0.5 ft²**
Outside Reactor Coolant System Control Volume				
1. Blowdown Enthalpy	180.11	179.61	179.09	171.98
2. Transferred to Steam Generator	- 2.93	- 1.08	- 0.84	8.76
	177.18	178.53	178.25	180.74
Inside Reactor Coolant System Control Volume				
1. Reactor Coolant Internal Energy Remaining in Vessel (plus accumulator addition)	4.18	4.98	6.97	17.44
2. Stored in Core	9.01	8.02	7.76	3.11
3. Core Internal Metal	3.26	2.91	2.61	0.00
4. Reactor Vessel Metal	5.91	5.91	5.91	5.91
5. Internal Energy of Water Remaining in Accumulator (injection not complete)	4.45	4.19	3.75	4.30
	26.81	26.03	27.00	30.76
	203.99	204.56	205.25	211.50

* All energy values in 10^6 Btu

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TABLE 14.3-7 INTERNAL ENERGY BALANCE*					
		Double Ended	**4.5 ft²**	**3.0 ft²**	**0.5 ft²**
U_i		4.15	4.15	4.15	4.15
Σ (mh)in	a/Blowdown	180.11	179.61	179.09	171.98
	b/Sprays	0.00	0.00	0.00	0.00
Σ Qin		~ 0	~ 0	~ 0	~ 0
Σ Qout	a/Structure	7.03	7.81	8.45	12.09
	b/Fans	0	0	0	0.61
Total U_f		177.23	175.95	174.79	163.83
From COCO the final conditions are:					
Steam		131.67	130.50	130.29	123.41
Air		4.11	4.10	4.10	4.05
Sump		40.27	40.66	40.03	37.18
		176.05	175.26	174.42	164.64

* All energy values in 10^6 Btu

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Table 14.3-8
Major Assumptions for Design Basis LOCA Analysis

1. Power level of 1683 MW(t).

2. Fraction of core inventory released [redacted]: noble gas 100%; iodines 50%.

3. Fraction of released iodines not plated out: 50%.

4. Composition of airborne iodine: 91% elemental, 4% organic, 5% particulate.

5. Filter efficiencies for iodines:
   a. Organic: 90% (charcoal)
   b. Elemental: 90% (charcoal)
   c. Particulate: 90% (HEPA)
   Used for shield building and auxiliary building filters

6. Primary containment leak rate:
   a. 0-24 hours: 0.5%/day
   b. 1-30 days: 0.25%/day

7. Leak pathway fractions of containment leakage

	0 to 6 Minutes	6 Minutes to 30 Days
Through shield building	0	.89
Through auxiliary building	.10	.10
Directly to environment	.90	.01

8. Building Volume

Containment vessel	1.32×10^6 ft³
Shielding building annulus	3.74×10^5 ft³
Auxiliary building special ventilation zone	1.0 ft³ (arbitrarily assigned to simulate a condition of no holdup)

9. A participation fraction of 0.5 is applied to the shield building annulus to account for non-uniform mixing in the annulus.

10. The Containment Vessel Internal Spray System starts to operate at 2.25 minutes post-LOCA. Iodine removal coefficients are given below (Ref. 2) and are based upon a boric acid/NaOH spray solution.

Iodine	$\lambda(hr^{-1})$	Decontamination Factor
Elemental	10.0	100
Organic	-	-
Particulate	0.45	100

Table 14.3-8
Major Assumptions for Design Basis LOCA Analysis

11. Air Flow Between Regions

	0 to 6 Min.	6 min. to 20 minutes	20 min. to 24 hours	After 24 hrs.
Filtered flow, cfm				
Q1(3,1)	-	5000	2700	2700
Q1(3,3)	-	-	2300	2300
Q1(4,1)	0.4583	0.4583	0.4583	0.22915
Unfiltered flow, cfm				
Q2(2,1)	4.1247	0.04583	0.04583	0.022915
Q2(2,3)	-	4.07887	4.07887	2.039435
Q2(2,4)	0.4583	0.4583	0.4583	0.22915

See Figure 14.3-35 for LOCADOSE region numbers. Q1(j,r) and Q2(j,r) are filtered and unfiltered flow, respectively, from node j to node r.

12. Meteorological Dispersion Coefficients

	Site Boundary (1200 m)	LPZ (4800 m)
0-2 hours	2.232E-4 sec/m³	3.977E-5 sec/m³
2-24 hours	-	4.100E-6
24-48 hours	-	2.427E-6
48-720 hours	-	4.473E-7

The dispersion coefficients have been derived from the on-site meteorology program and are discussed in Section 2.7.4 and illustrated in Figure 2.7-8. The releases are assumed at ground level with a correction for building wake effects.

13. Breathing Rate		
0-8 hours	3.47x10[redacted]	m³/sec
8-24 hours	1.75x10[redacted]	m³/sec
24-720 hours	2.32x10[redacted]	m³/sec

Table 14.3-8B
Activity in the Core for Design Basis LOCA
At the Kewaunee Nuclear Power Plant for Advanced Nuclear Fuels

* End of Cycle conditions, 850 days of operation at 1683 MWt.
* Fuel assembly exposure of 49,000 MWD/MTU.
* ORIGIN computer code was used to determine isotopic composition and activity (curies).

Isotope	Activity	Isotope	Activity
I-129	1.60	Kr-88	2.98×10^7
I-131	4.84×10^7	Kr-89	3.71×10^7
I-132	6.87×10^7	Xe-131m	3.50×10^5
I-133	9.04×10^7	Xe-133	8.99×10^7
I-134	1.03×10^8	Xe-133m	2.19×10^6
I-135	8.00×10^7	Xe-135	1.74×10^7
Kr-85	5.02×10^5	Xe-135m	2.42×10^7
Kr-85m	1.06×10^7	Xe-137	8.77×10^7
Kr-87	2.04×10^7	Xe-138	8.45×10^7

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TABLE 14.3-9 DOSES FOR DESIGN BASIS LOSS-OF-COOLANT ACCIDENT		
Base Case: 2700 CFM exhaust rate		
	Thyroid Dose	Whole Body Dose
2-Hour Exposure (site boundary)	37.1 rem	1.7 rem
30-Day Exposure (low population zone)	7.3 rem	0.4 rem
10 CFR 100 Limit	300.0 rem	25.0 rem
No Recirculation Case: 5000 CFM exhaust rate		
	Thyroid Dose	Whole Body Dose
2-Hour Exposure (site boundary)	39.4 rem	2.1 rem
30-Day Exposure (low population zone)	8.0 rem	0.5 rem
10 CFR 100 Limit	300.0 rem	25.0 rem

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TABLE 14.3-10
EXPOSED ZINC AND ALUMINUM BEARING SURFACES
WITHIN KEWAUNEE CONTAINMENT VESSEL

	Square Feet	Thickness or Weight/Area	Pounds of Metal
Galvanized Steel Surfaces			
Platform and gratings, all surfaces	12,400	2.0 oz Zn/ft²	1,550
All ductwork, both sides	35,253	0.60 oz Zn/ft²	1,322
Conduit, trays and supports exposed to spray	13,250	0.60 oz Zn/ft²	497
Stair treads	2,305	0.60 oz Zn/ft²	86
Cable restraints	338	--	68
Total Galvanized	63,596		3,523
Zinc-Bearing Undercoats			
Primer on structural steel, Carbozine 11	95,400	3 mils specified	5,540
Primer on containment vessel interior, Carbozine 11	51,500	3 mils specified	2,243
Total Undercoat	146,900		8,530
Aluminum Surfaces			
Reactor equipment, including contingency	144.5	--	424
Crane lights and fixtures	Undetermined	--	160
Connectors on rod drive cables	58.7	--	110
Total Aluminum	Undetermined		694
Aluminum Paint on Reactor Equipment	Undetermined	--	110

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TABLE 14.3-11 HYDROGEN GAS PRODUCTION IN ACIDIC AND BASIC SOLUTIONS				
	First Day Reaction Rate	**Area or Weight**	**First Day scf**	**Continuing scf/day**
No additive (pH < 7)				
Zinc-bearing paint	2.3 cc/cm²	146,900/2 ft²	5542	
Galvanized surface	2.3 cc/cm²	63,596/2 ft²	2400	36.4
Total				36.4
With additive (pH > 7)				
Zinc-bearing paint	2.0 cc/cm²	146,900/2 ft²	4819	
Galvanized surface	low	63,596/2 ft²	--	36.4
Aluminum paint	all	110 lb	2200	
Aluminum structure	1 mil	694 lb	66	36.4
Total			7085	72.8

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TABLE 14.3-12 SOURCES AND ASSUMPTIONS FOR HYDROGEN CALCULATIONS		
	Reasonably Conservative Estimate	**Most Conservative Estimate Using Safety Guide 7**
Coolant Absorption of Radiation from Fuel		
Halogens in fuel	50%	50%
Noble gases in fuel	100%	0
Other fission products in fuel	99%	99%
Gamma energy fraction absorbed in water	.071	.10
Beta energy fraction absorbed in water	0	0
$G(H_2)$, molecules/100 ev	0.44	0.50
Sources in Coolant		
Halogens in coolant	50%	50%
Noble gases in coolant	0	0
Other fission products in coolant	1%	1%
Gamma energy fraction absorbed	1.0	1.0
Beta energy fraction absorbed	1.0	1.0
$G(H_2)$ molecules/100 ev	0.30	0.50
Initial Zirconium-Water Reaction	2% (4862 scf)	5% (12,155 scf)
Initial Zinc Reaction		
1.15 cc/cm² x 200, 146 ft²	7550 scf	7550 scf
Continuing Zinc Reaction	30.5 scf/day	30.5 scf/day
Plant Characteristics		
Thermal power	1721.4 Mw	
Containment free volume	1,320,000 ft³	
Initial containment temperature	120°F	
Zirconium cladding	30,858 lbs	

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TABLE 14.3-13 VENTING REQUIREMENTS AND VENTING DOSES WITHOUT PRESSURE INCREASE		
	Reasonably Conservative Estimate	**Most Conservative Estimate Using Safety Guide 7**
Time of occurrence of 3.5 v/o hydrogen	41 days	10.1 days
Time of occurrence of 4.1 v/o hydrogen	65 days	17 days
Initial vent rate necessary to maintain 3.5 v/o hydrogen	7.3 scfm or 0.90% day at 41 days	24.2 scfm or 3.05% day at 10.1 days
Occurrence of 1 MPC at site boundary*	63 days *	60 days
Initial 1 MPC vent flow at time indicated above	5.8 scfm	9.3 scfm
Earliest completion of purge with 1 MPC maintained at site boundary	122 days	98 days
Venting dose at site boundary up to 30 post-accident days	--	6.37 rem thyroid [a]; .75 rem whole body
Venting dose at site boundary up to occurrence of 1 MPC	.166 rem thyroid .017 rem whole body	6.88 rem thyroid [b] .79 rem whole body
Venting dose at 4800 meters up to 30 post-accident days	--	.734 rem thyroid; .087 rem whole body
Venting dose at 4800 meters up to Occurrence of 1 MPC at site boundary	19.1 mrem thyroid; 2.0 mrem whole body	.792 rem thyroid; .091 rem whole body

* In the reasonable conservative case, a 1 MPC vent capability would occur at 63 days and 4.07% hydrogen concentration if venting were deferred and not initiated at 3.5% concentration.

[a] Dose is 3.02 rems for an SBVS filter efficiency of 95%.

[b] Dose is 3.25 rems for an SBVS filter efficiency of 95%.

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TABLE 14.3-14
EFFECTS OF PRESSURE INCREASE ON VENTING DOSES

	No Deferment of Venting		Venting Deferred Until 30 Days		Venting Deferred Until 1 MPC	
Occurrence of 3.5 % Hydrogen Concentration	10.1 days		10.1 days		10.1 days	
Action at 10.1 days	Vent at 24.2 scfm initial		Compress hydrogen with 24.2 scfm air initial		Compress hydrogen with 24.2 scfm air initial	
Stop Pressure Rise and Begin Venting	--		30 days		30 days	
Peak Containment Pressure	0 psig		6.1 psig		12.2 psig	
Occurrence of 1 MPC at site Boundary	60 days		62 days		63 days	
Earliest Completion of 1 MPC Final Purge	98 days		119 days		131 days	
Site Boundary Doses up to Occurrence of 1 MPC: From Venting: Thyroid Dose Whole Body Dose	6.88 rem*	0.79 rem	0.64 rem	0.049 rem	0 rem	0 rem
From Pressure-Induced Leakage Before Venting:** Thyroid Dose Whole Body Dose	0 rem	0.72 rem 0 rem	0.99 rem	0.075 rem		0.097 rem
TOTAL DOSE Thyroid Dose Whole Body Dose	6.88 rem*	0.79 rem	1.36 rem	0.12 rem	0.99 rem	0.097 rem

* Dose is 3.25 rems for an SBVS filter efficiency of 95%.
** Based on 0.01/day leakage at design test pressure.

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TABLE 14.3-15
STEAM GENERATOR TUBE REMOVAL

Steam Generator	Row	Column	Extent Removed
B	11	9	Below TSP 4H
B	4	81	Below TSP 4H
B	25	73	Below TSP 4H
B	16	29	Below TSP 2C
B	13	41	Below TSP 3C
B	16	42	Below TSP 3C
B	1	32	Below TSP 1H
B	2	32	Below TSP 1H
B	1	54	Below TSP 1H
B	2	54	Below TSP 1H
B	1	61	Below TSP 1H
B	2	61	Below TSP 1H
A	1	25	Below TSP 1H
A	2	25	Below TSP 1H
A	1	21	Below TSP 1H
A	2	21	Below TSP 1H
B	1	21	Below TSP 1H
B	2	21	Below TSP 1H
B	1	51	Below TSP 1H
B	2	51	Below TSP 1H
B	1	58	Below TSP 1H
B	2	58	Below TSP 1H
B	1	65	Below TSP 1H
B	2	65	Below TSP 1H
B	1	69	Below TSP 1H
B	2	69	Below TSP 1H
B	1	40	Below TSP 1H
B	2	40	Below TSP 1H
A	15	57	Below TSP 1H
A	29	41	Below TSP 1H
A	29	51	Below TSP 1H
A	22	54	Below TSP 1H
A	28	28	Below TSP 1H
A	23	40	Below TSP 1H
A	1	30	Below TSP 1H
A	2	30	Below TSP 1H
B	15	28	Below TSP 3C

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SCRAM REACTIVITY INSERTION RATE

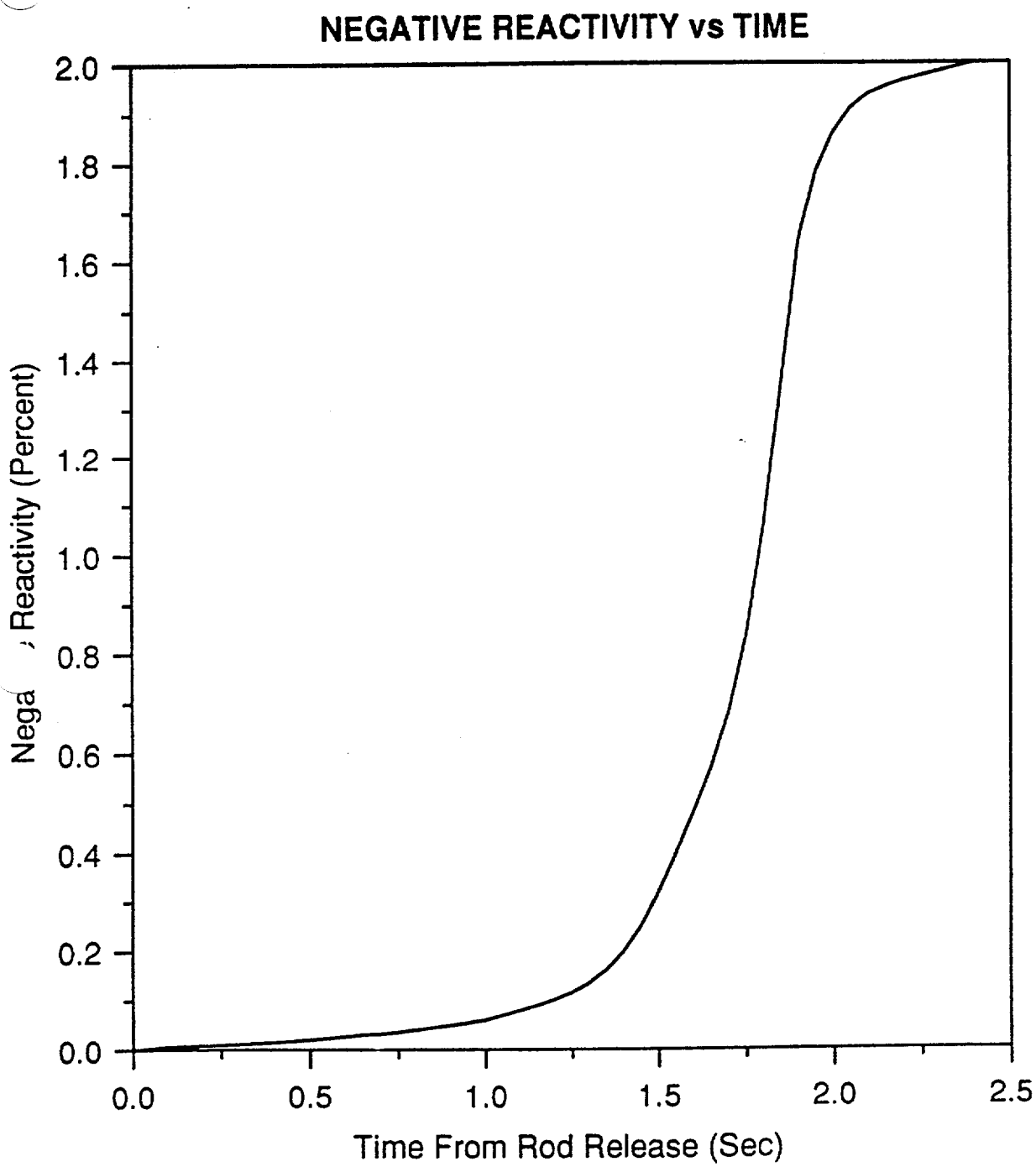

FIGURE 14.0.1

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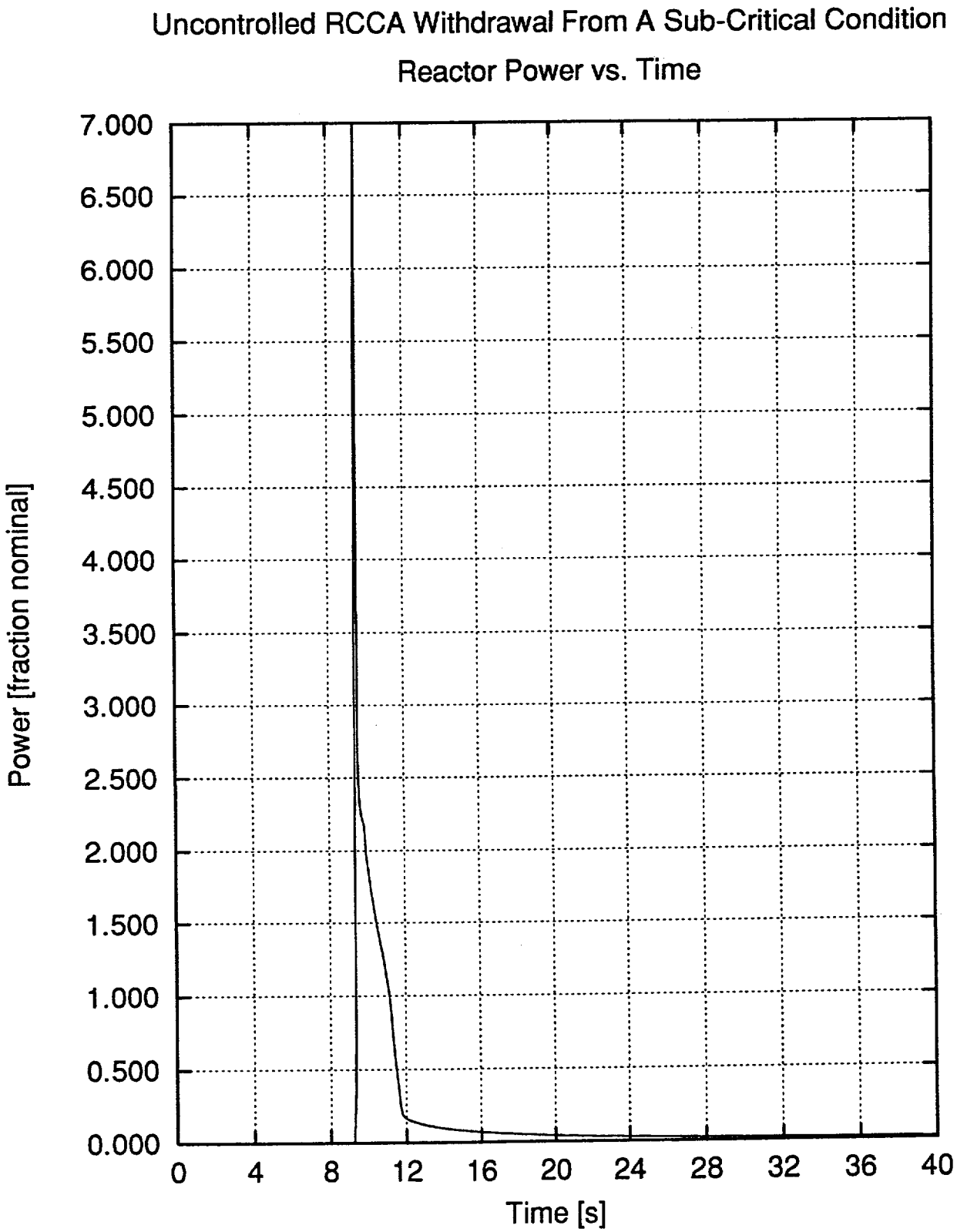

Figure 14.1.1-1

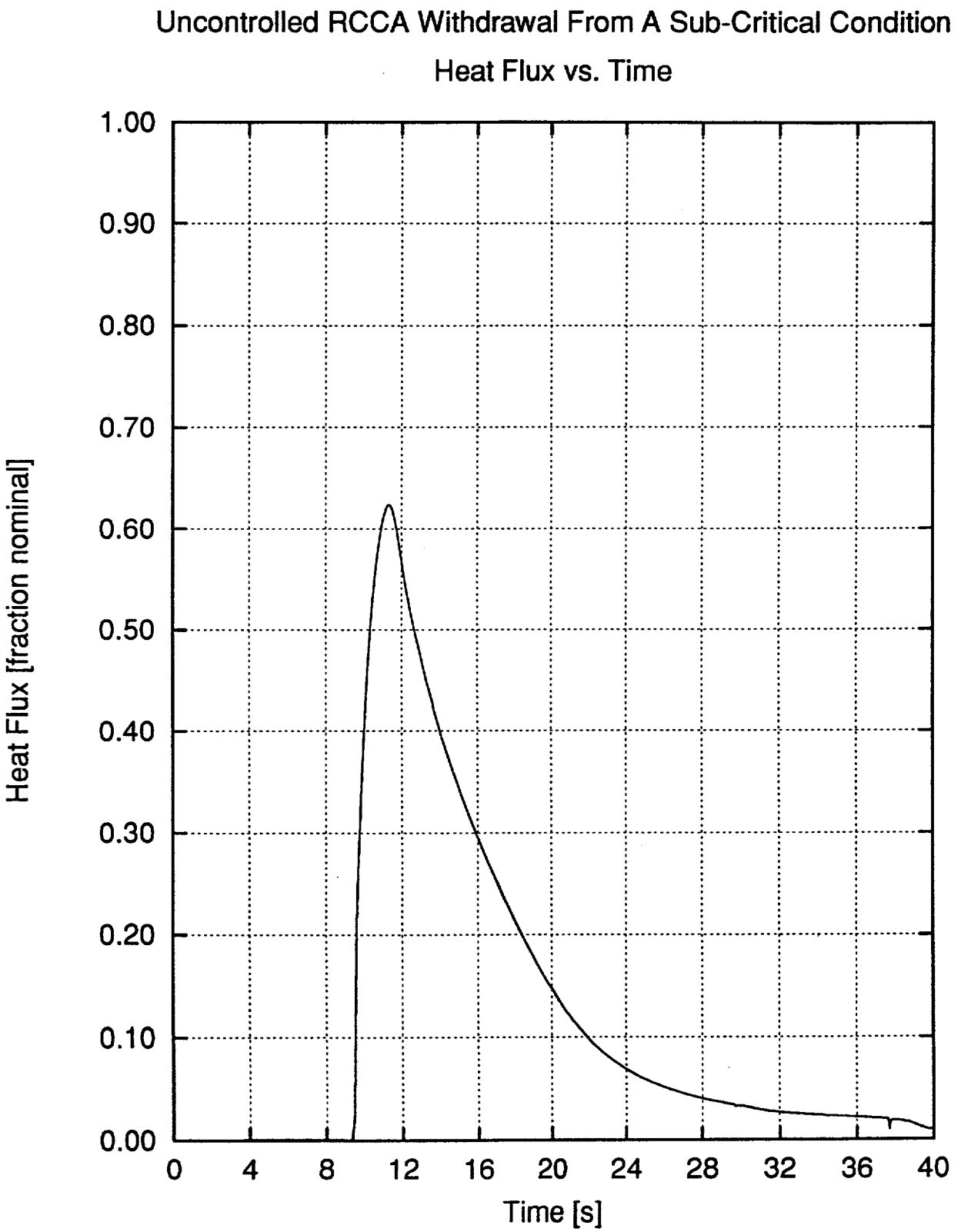

Figure 14.1.1-2

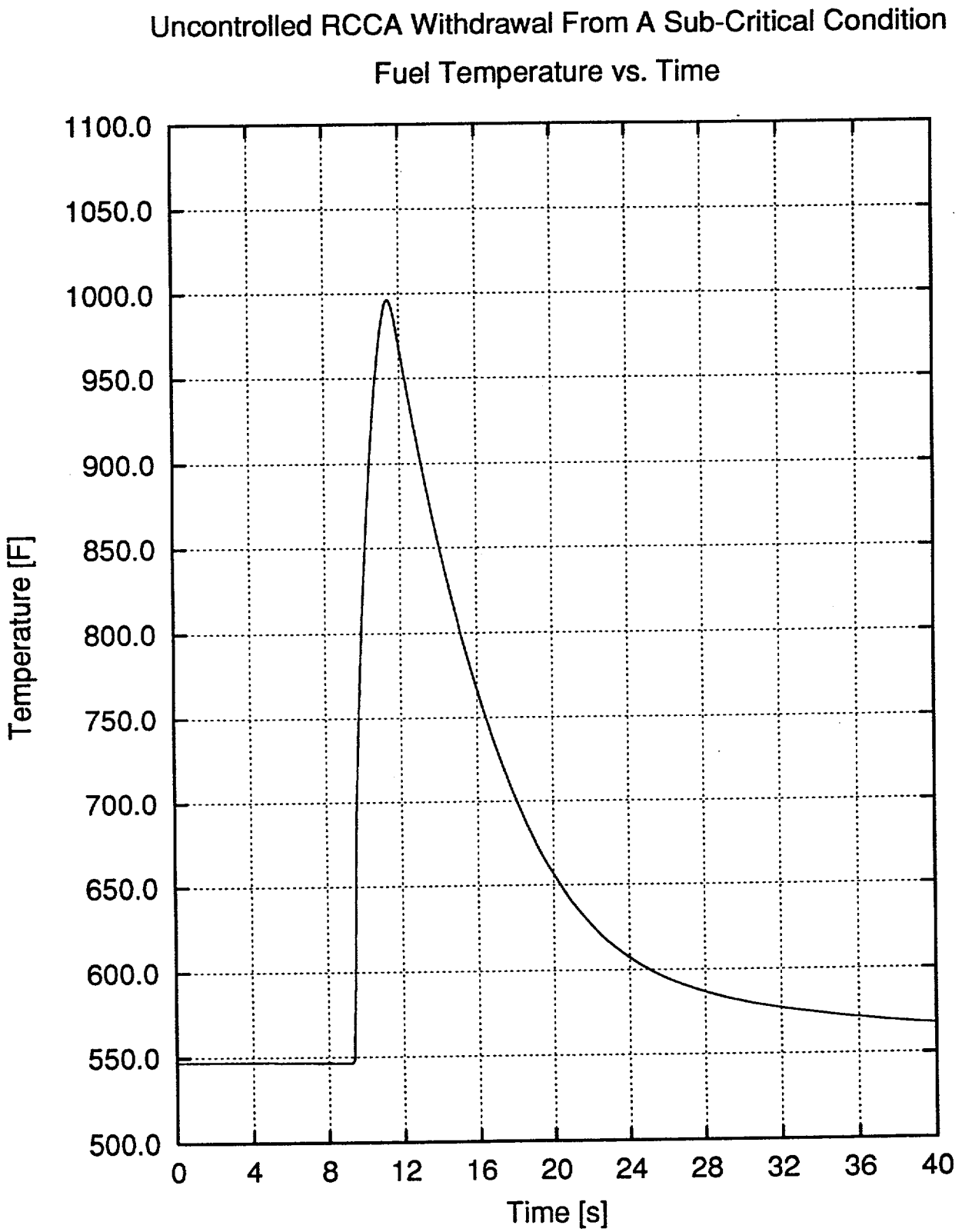

Figure 14.1.1-3

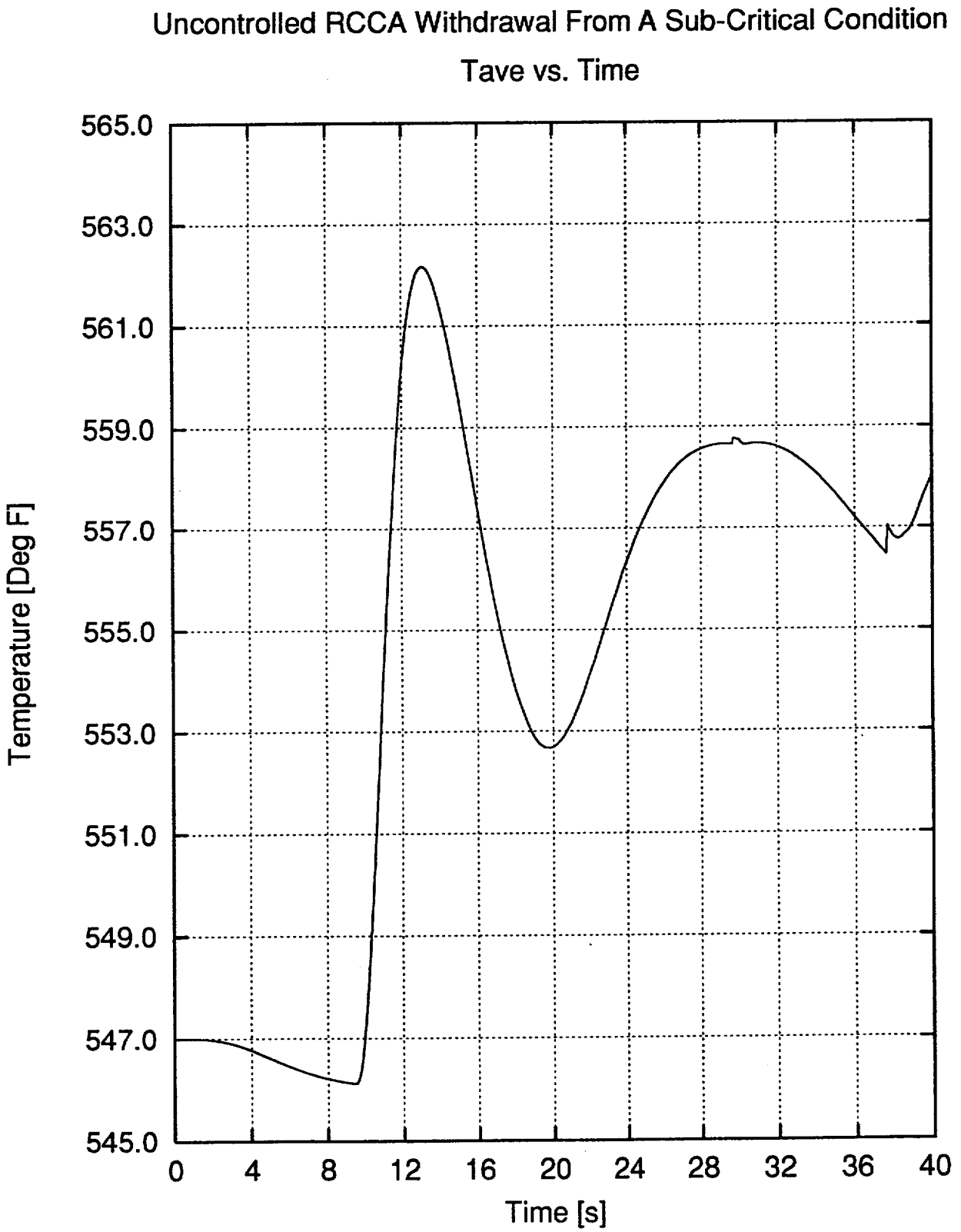

Figure 14.1.1-4

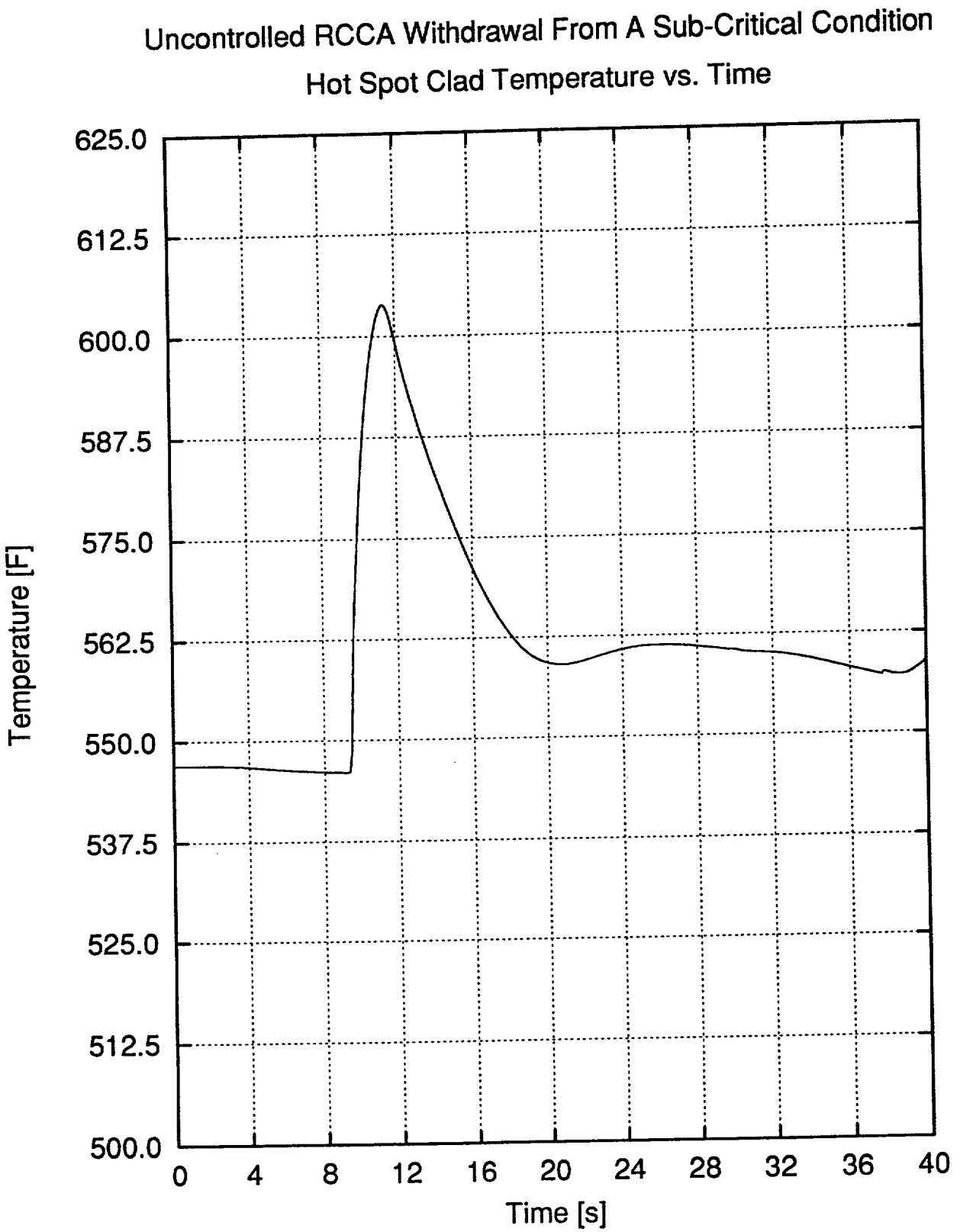

Figure 14.1.1-5

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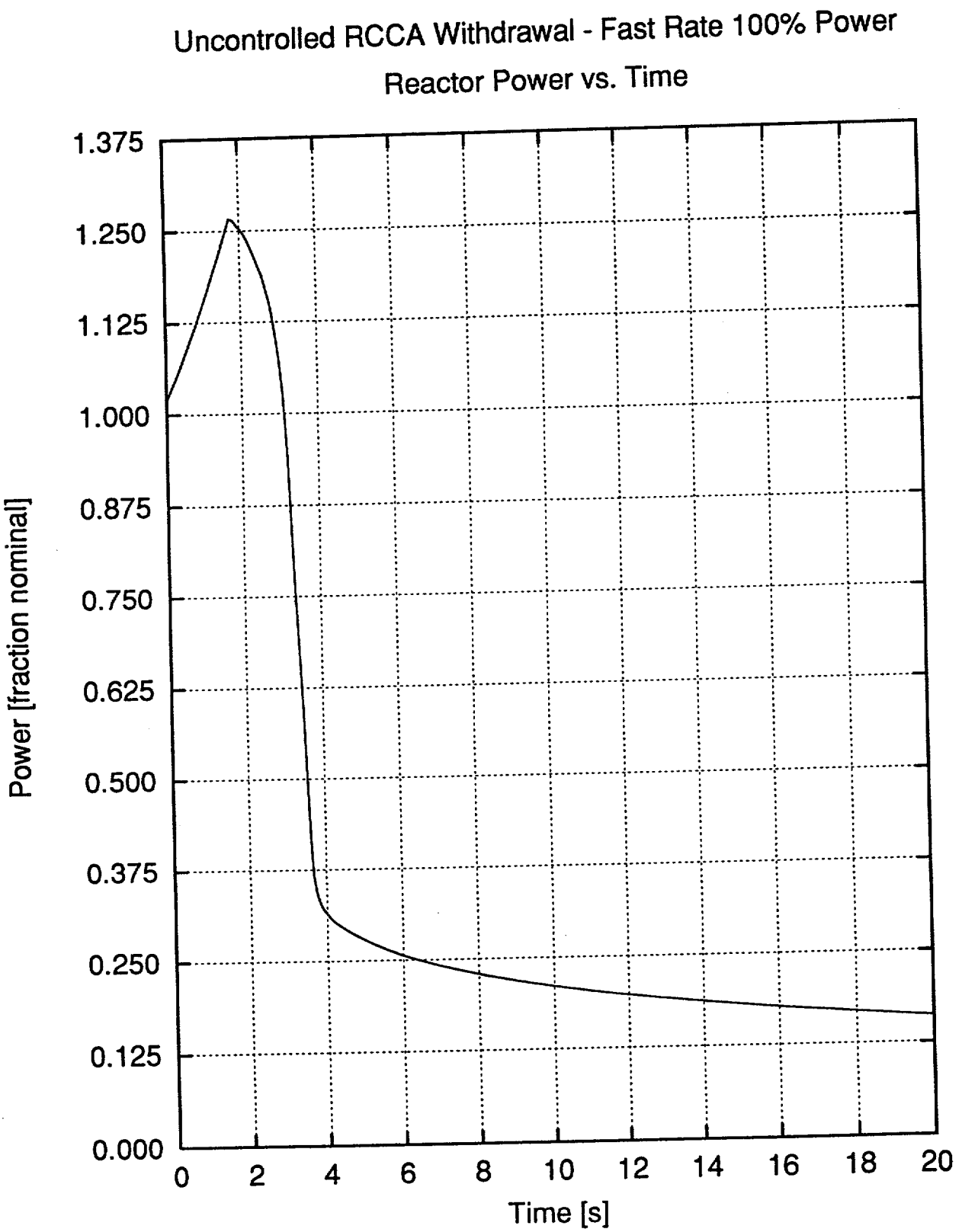
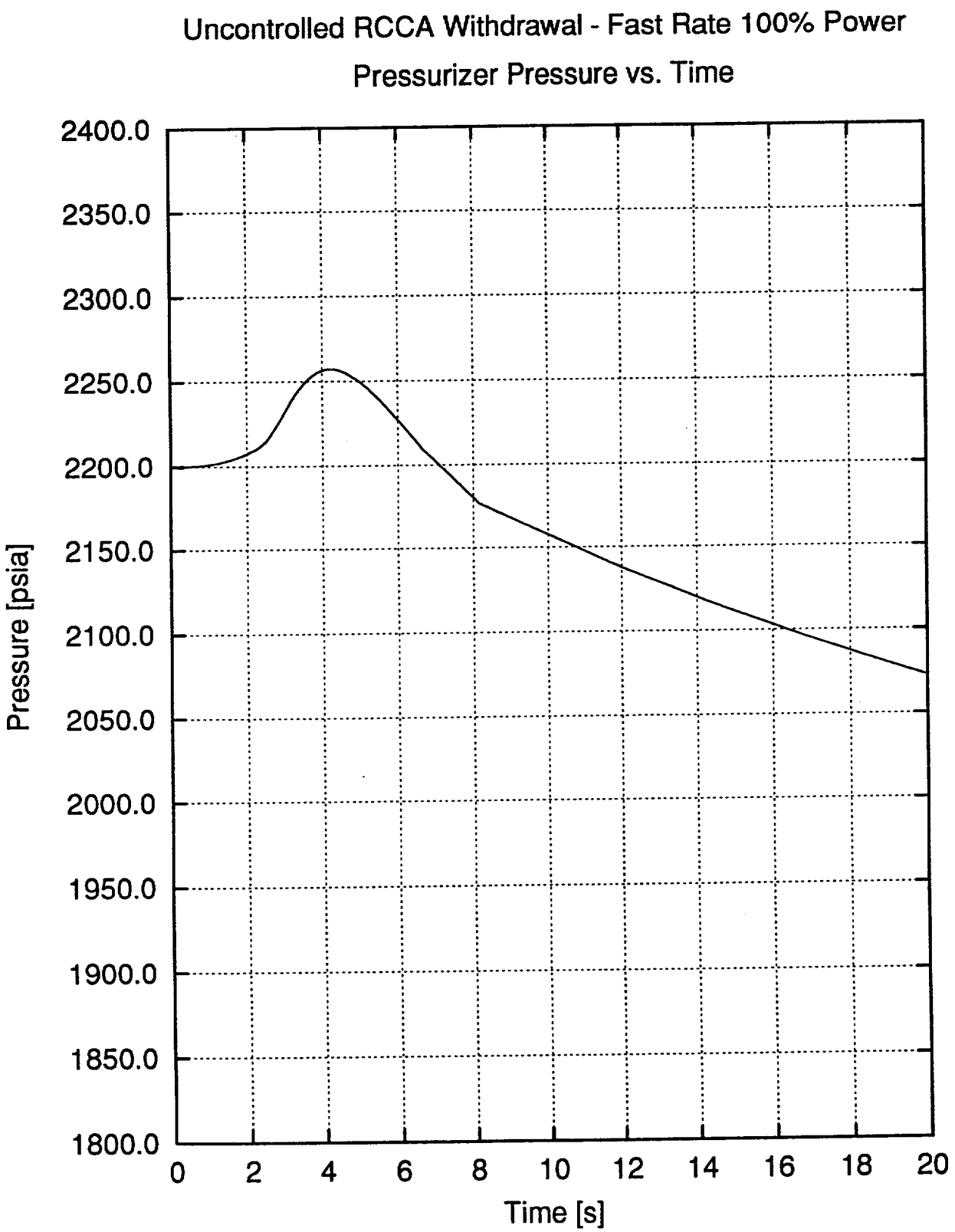


Figure 14.1.2-1



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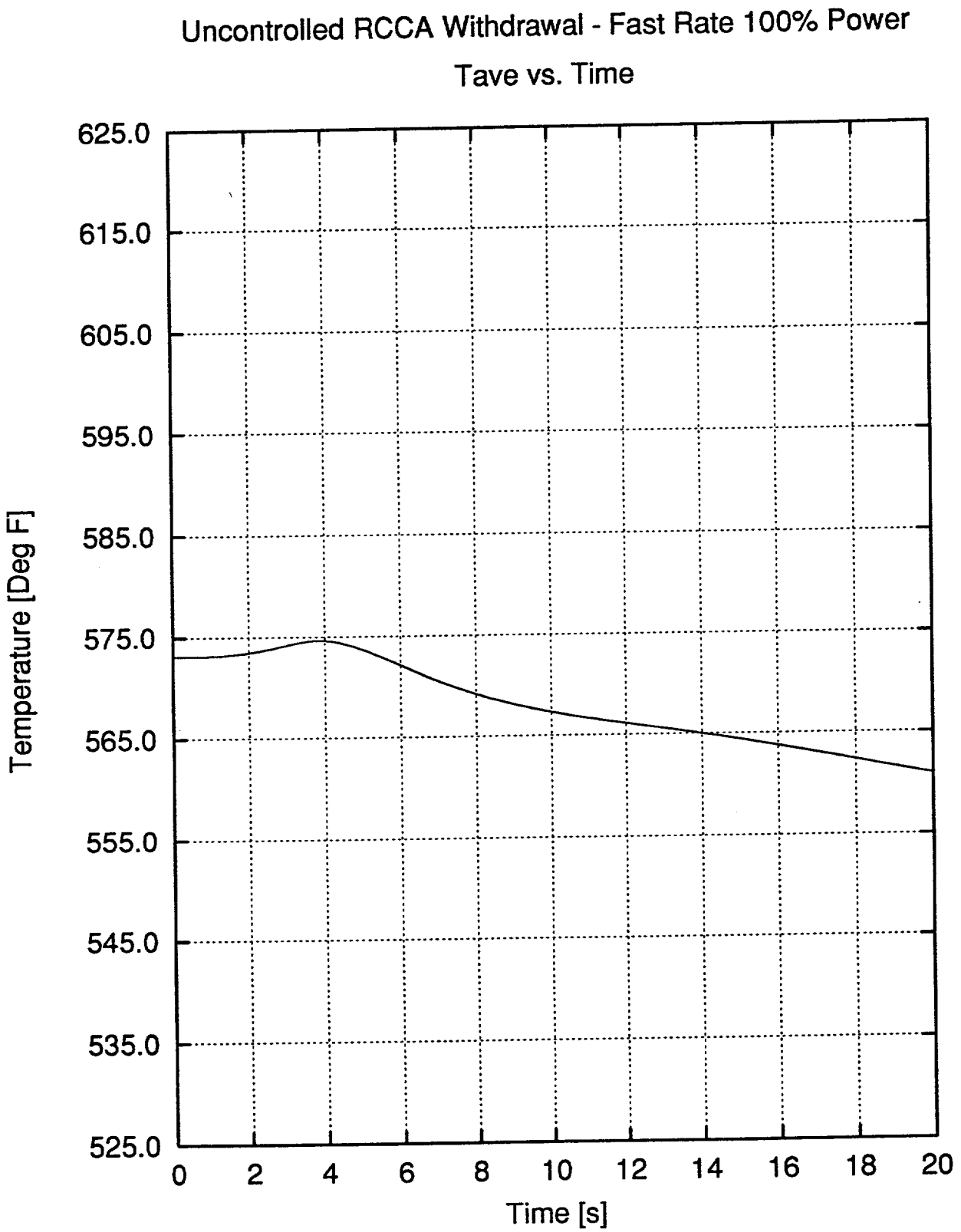

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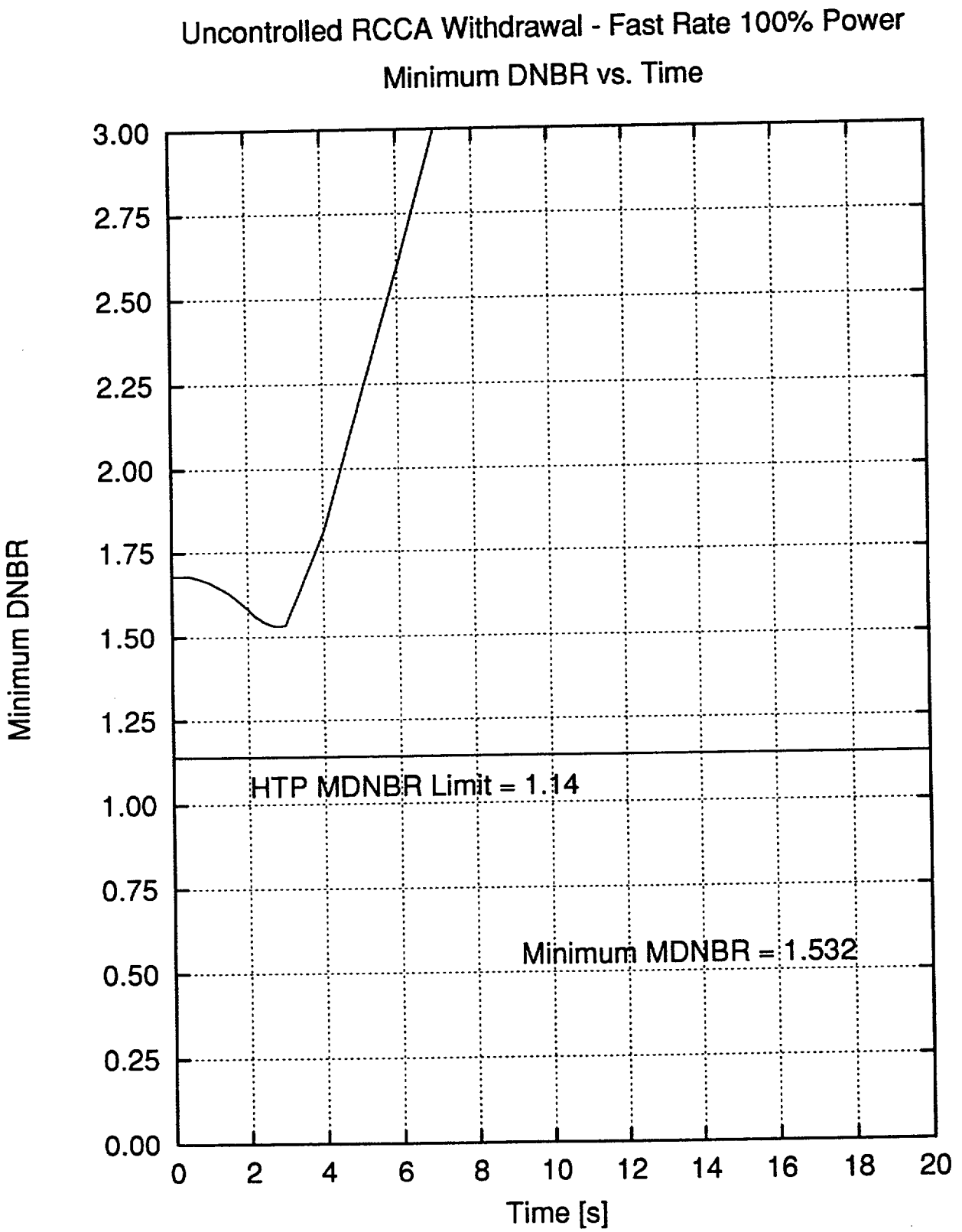

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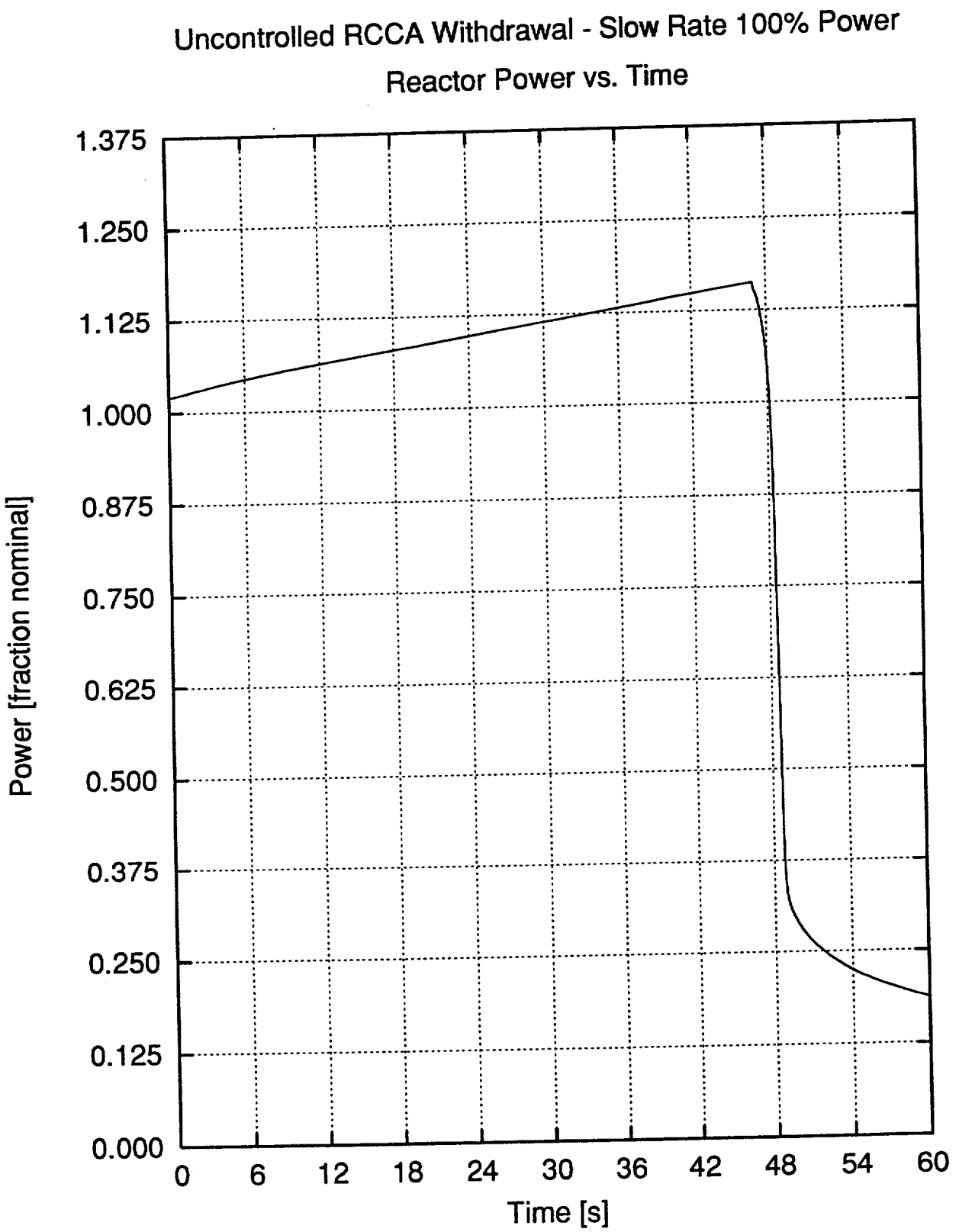

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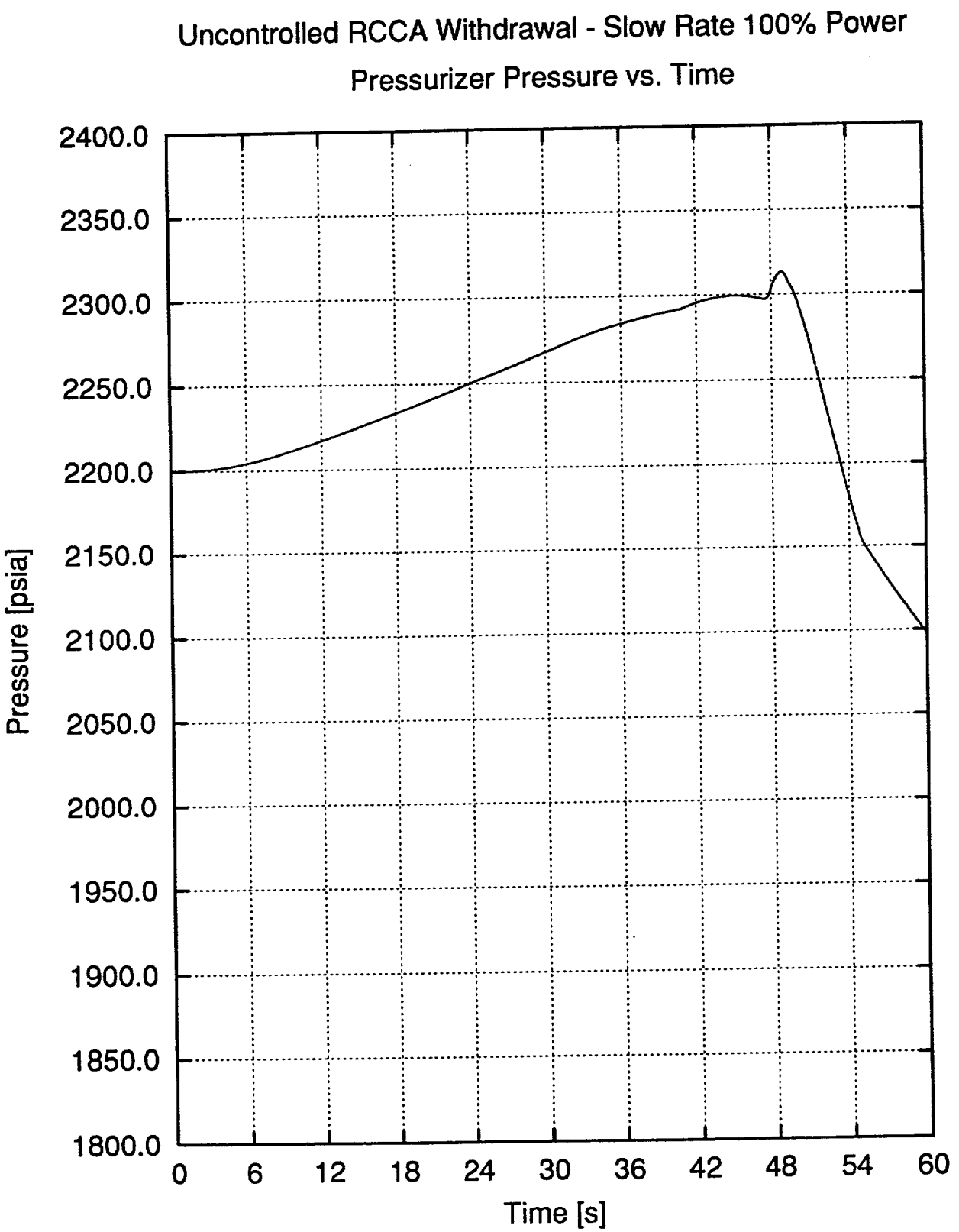


Figure 14.1.2-6

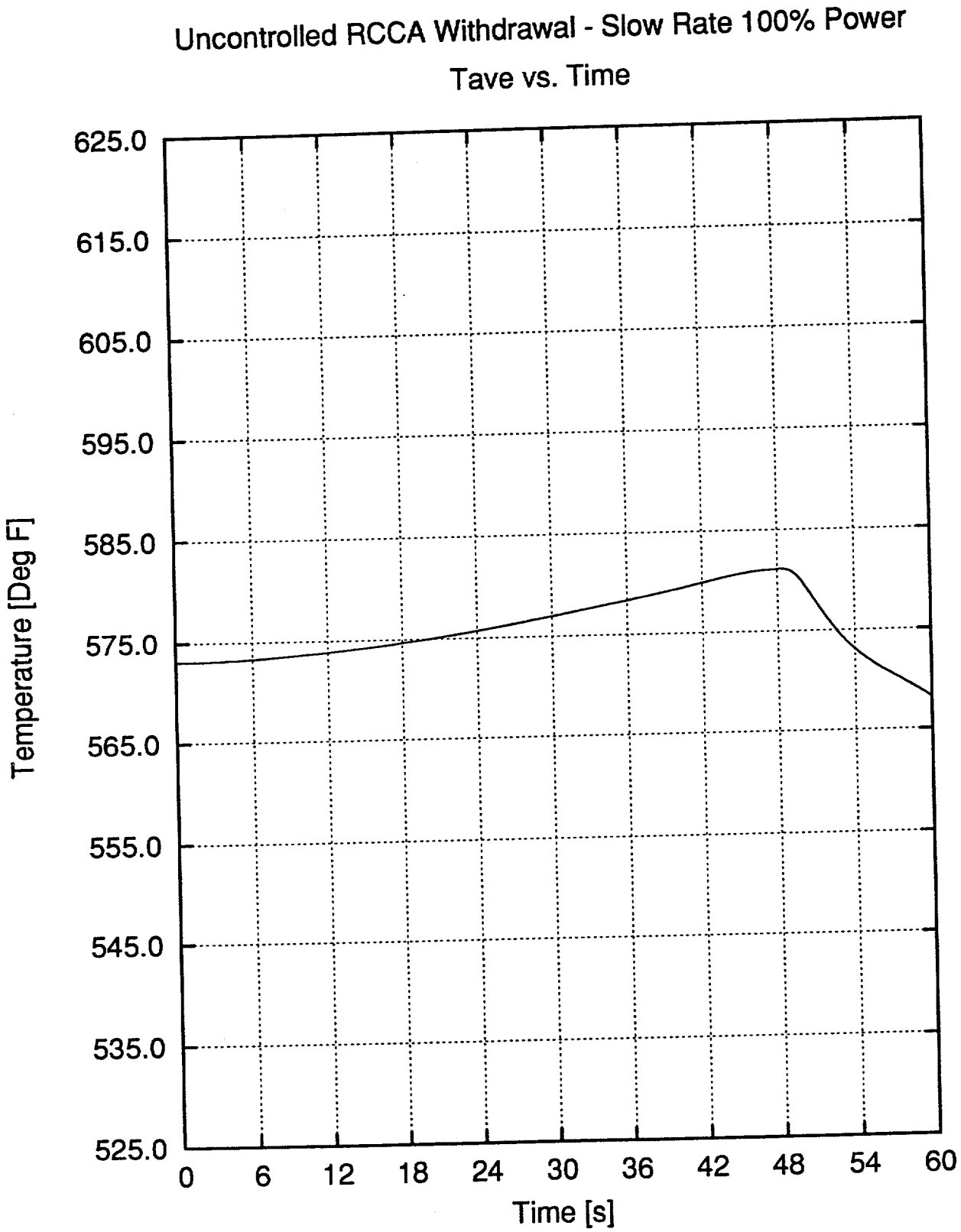


Figure 14.1.2-7

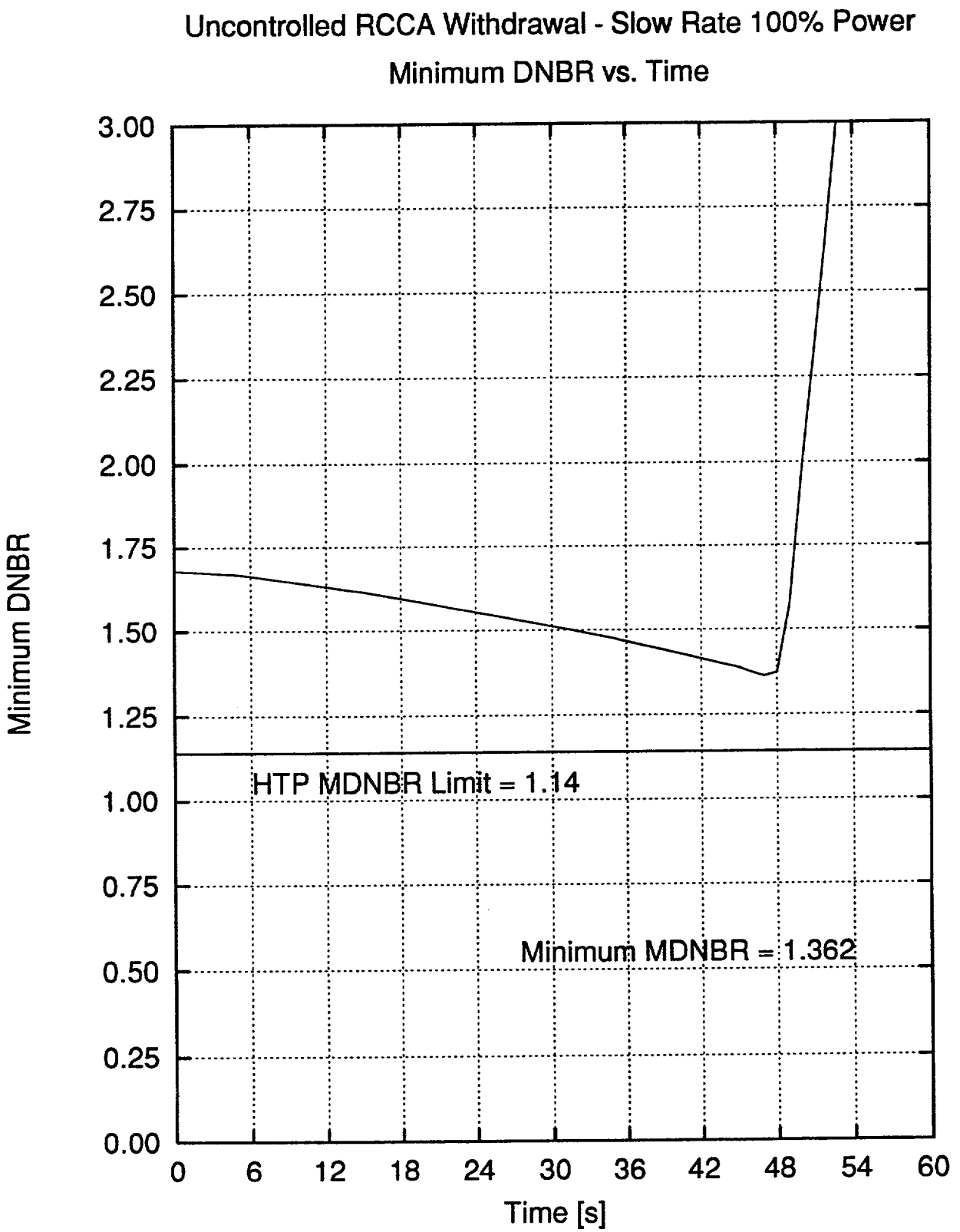


Figure 14.1.2-8

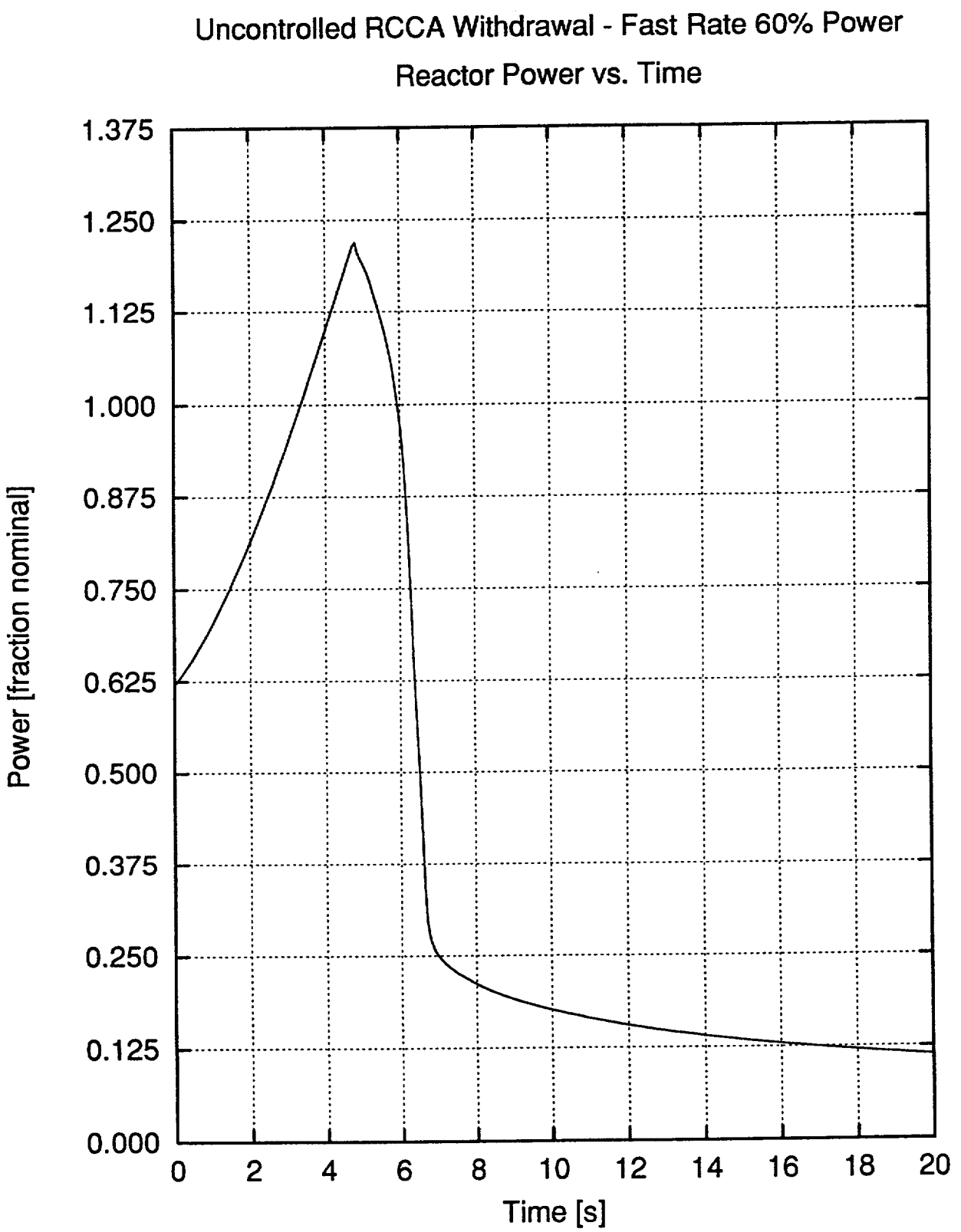


Figure 14.1.2-9

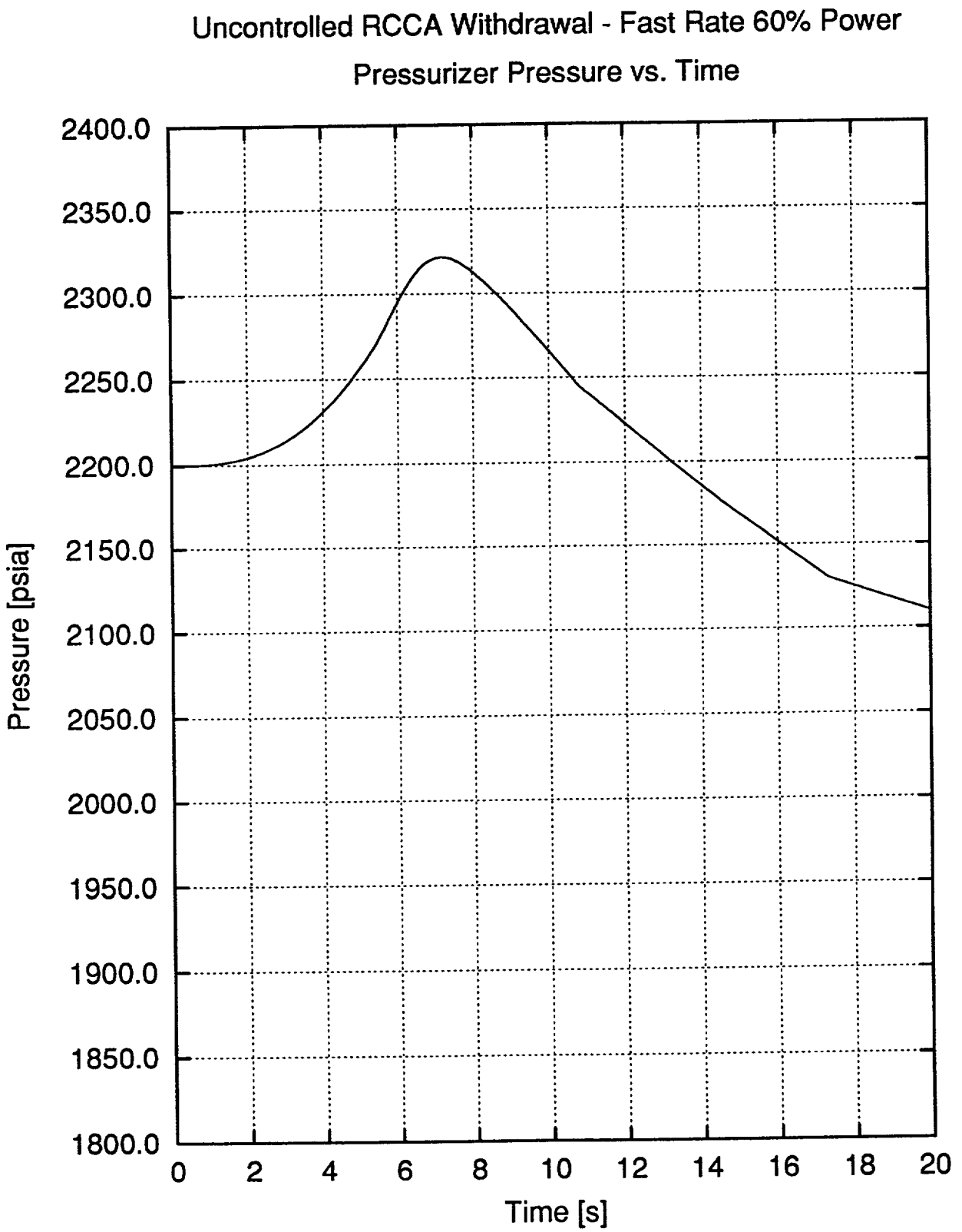


Figure 14.1.2-10

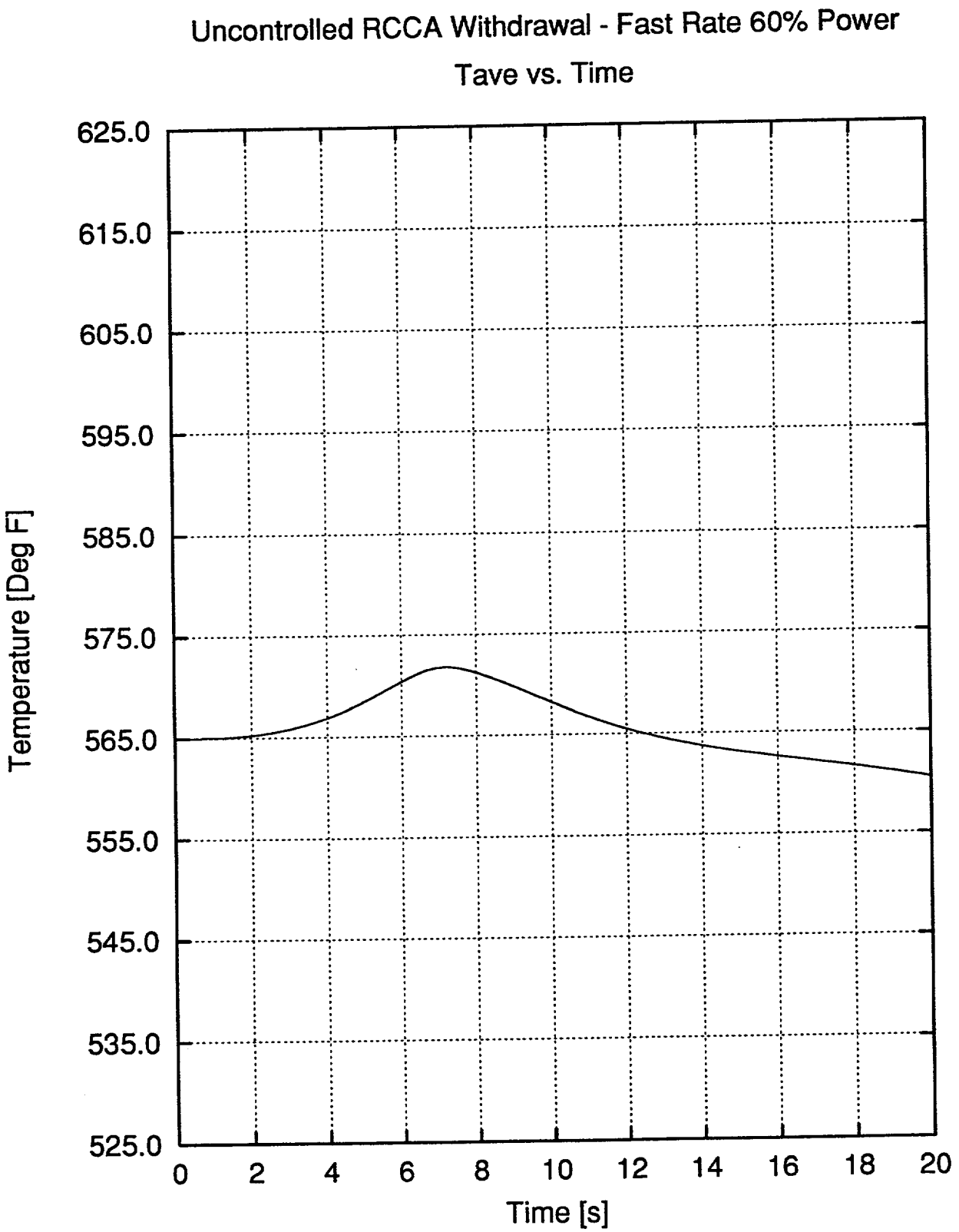


Figure 14.1.2-11

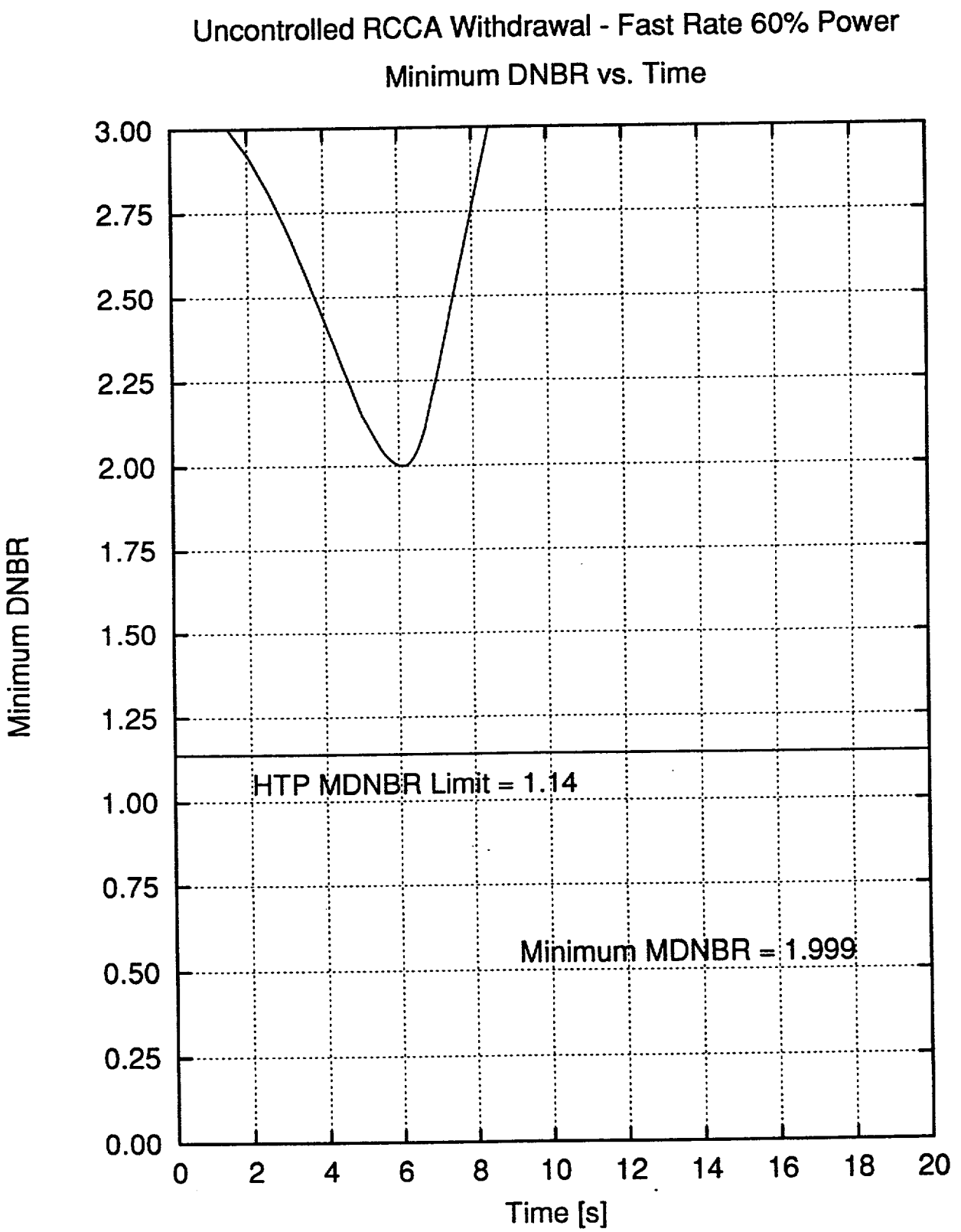


Figure 14.1.2-12

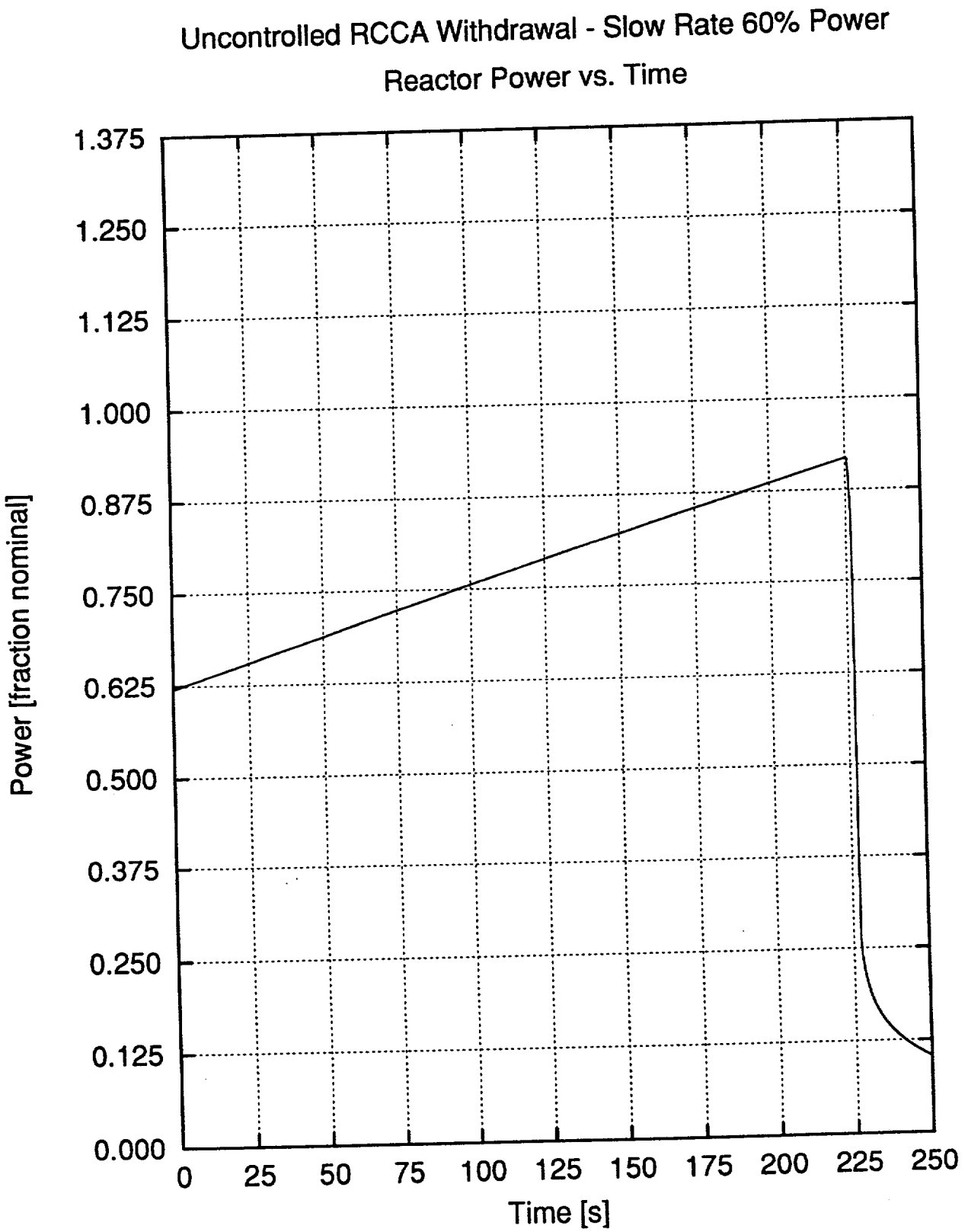


Figure 14.1.2-13

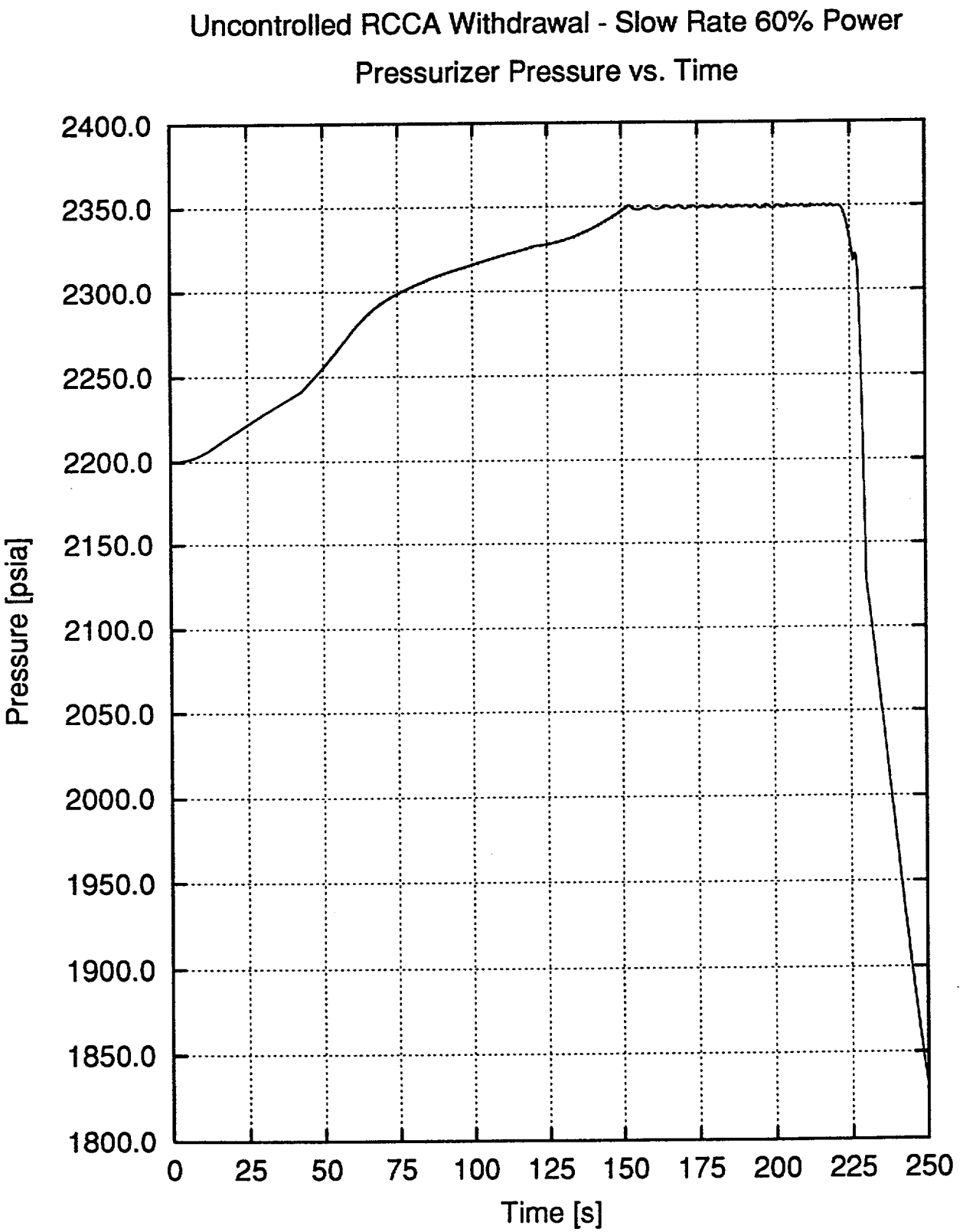
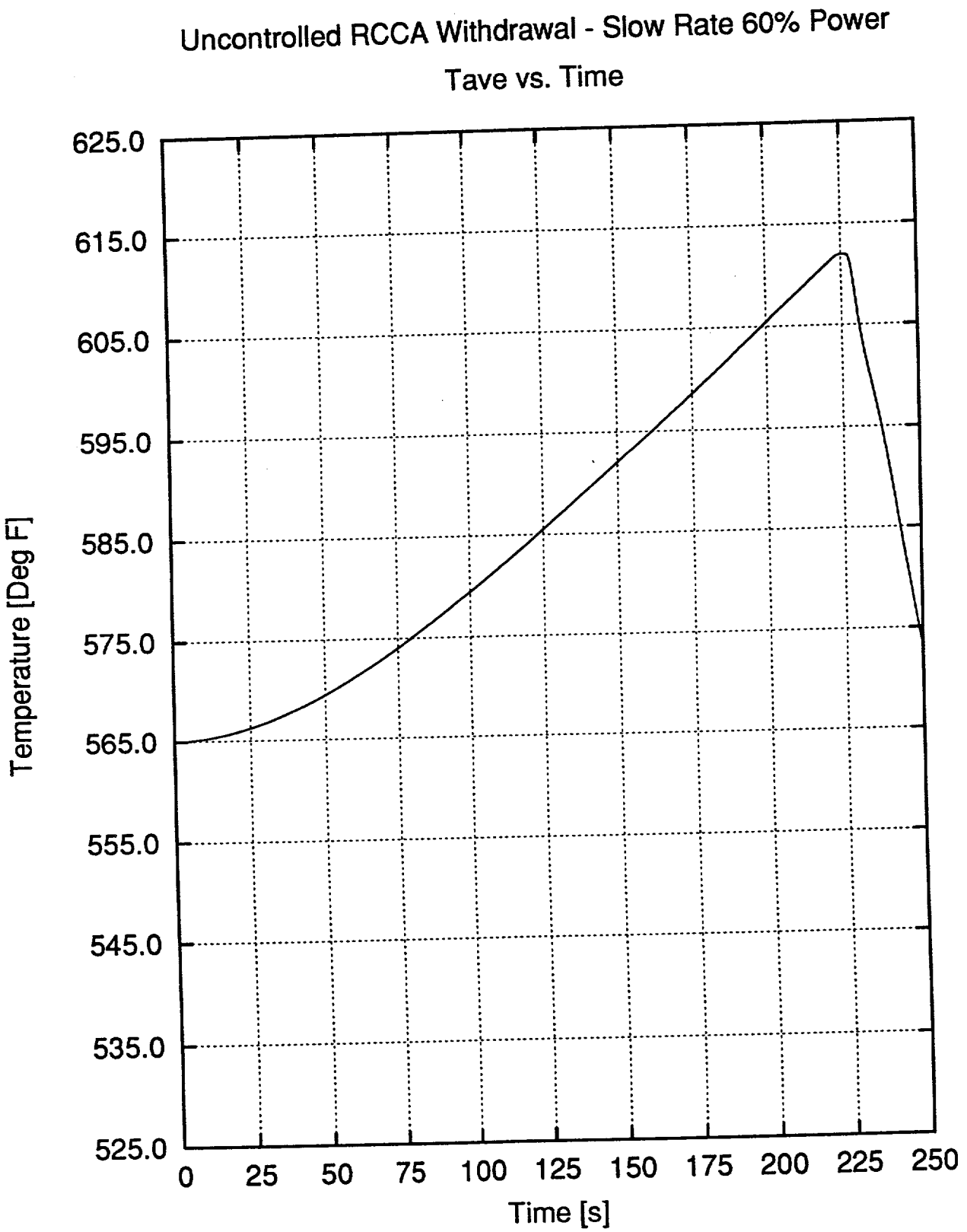


Figure 14.1.2-14



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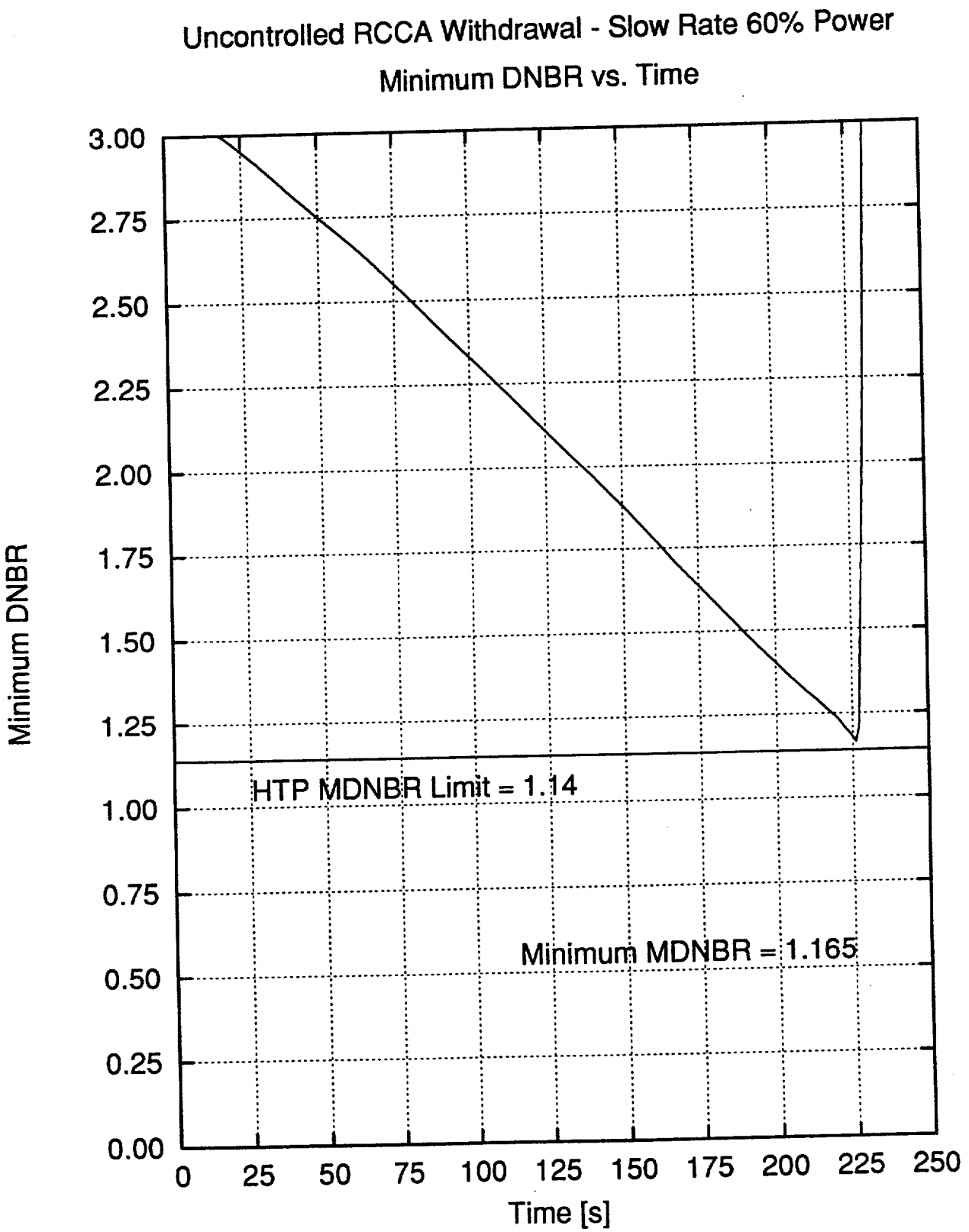


Figure 14.1.2-16

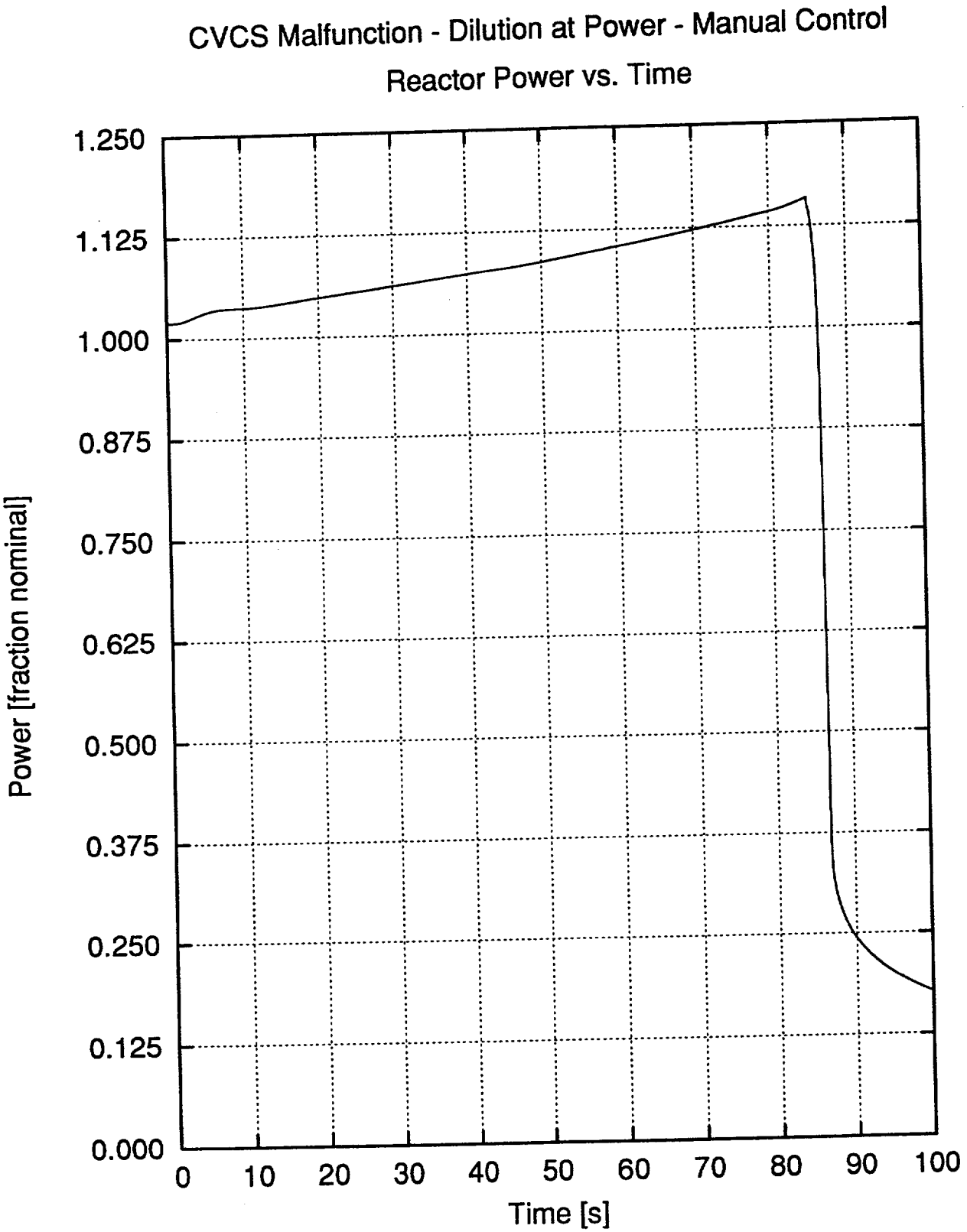
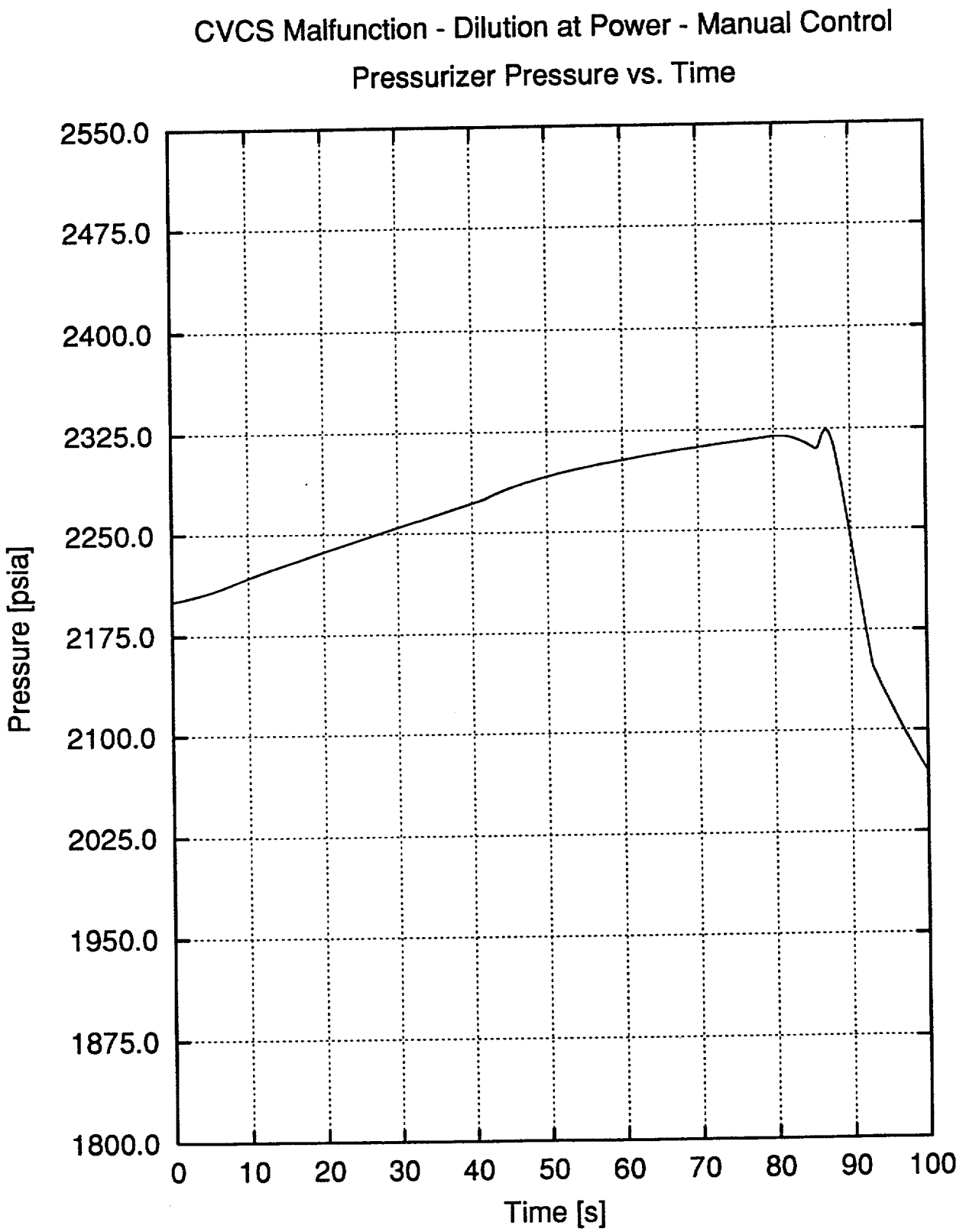


Figure 14.1.4-1



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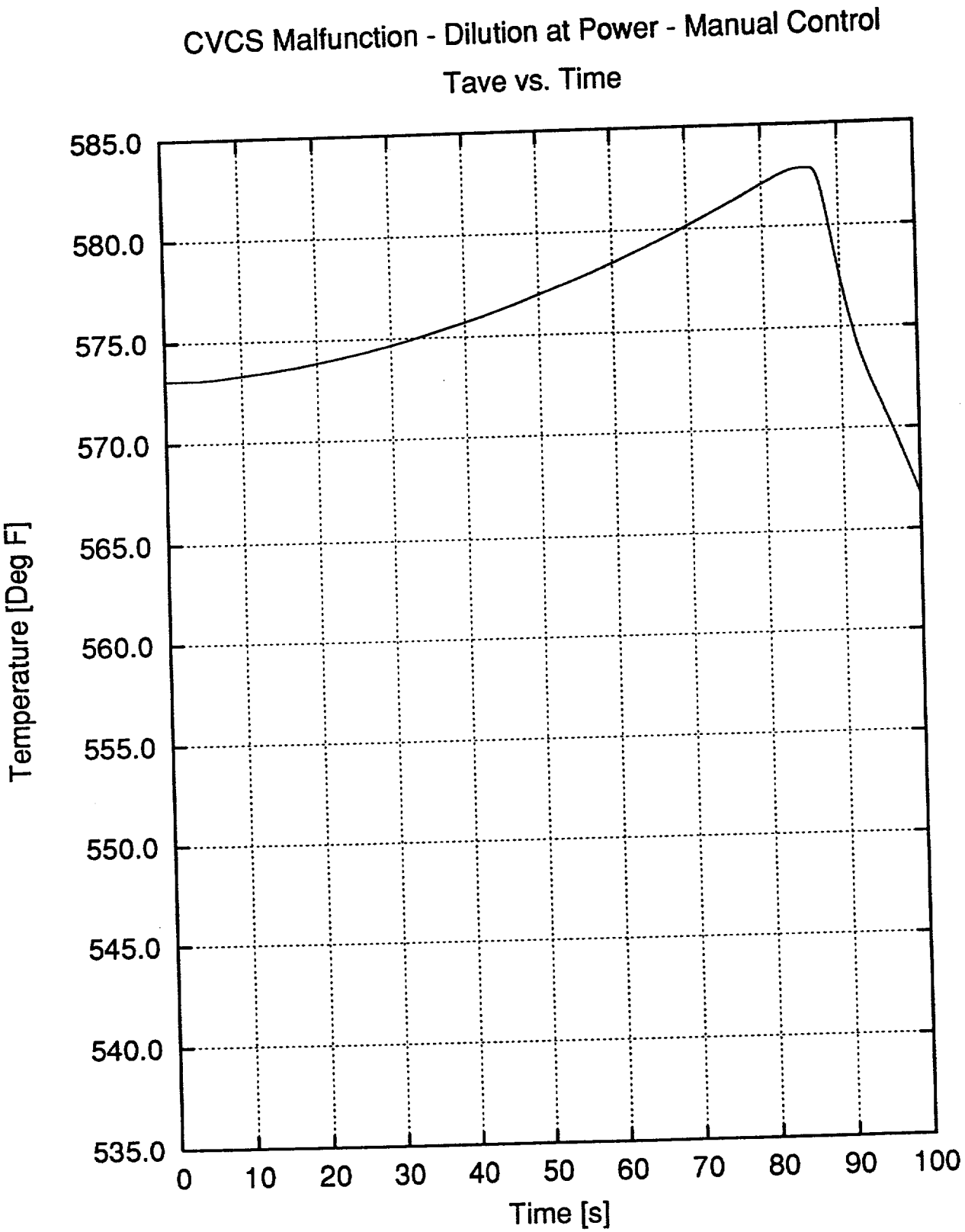

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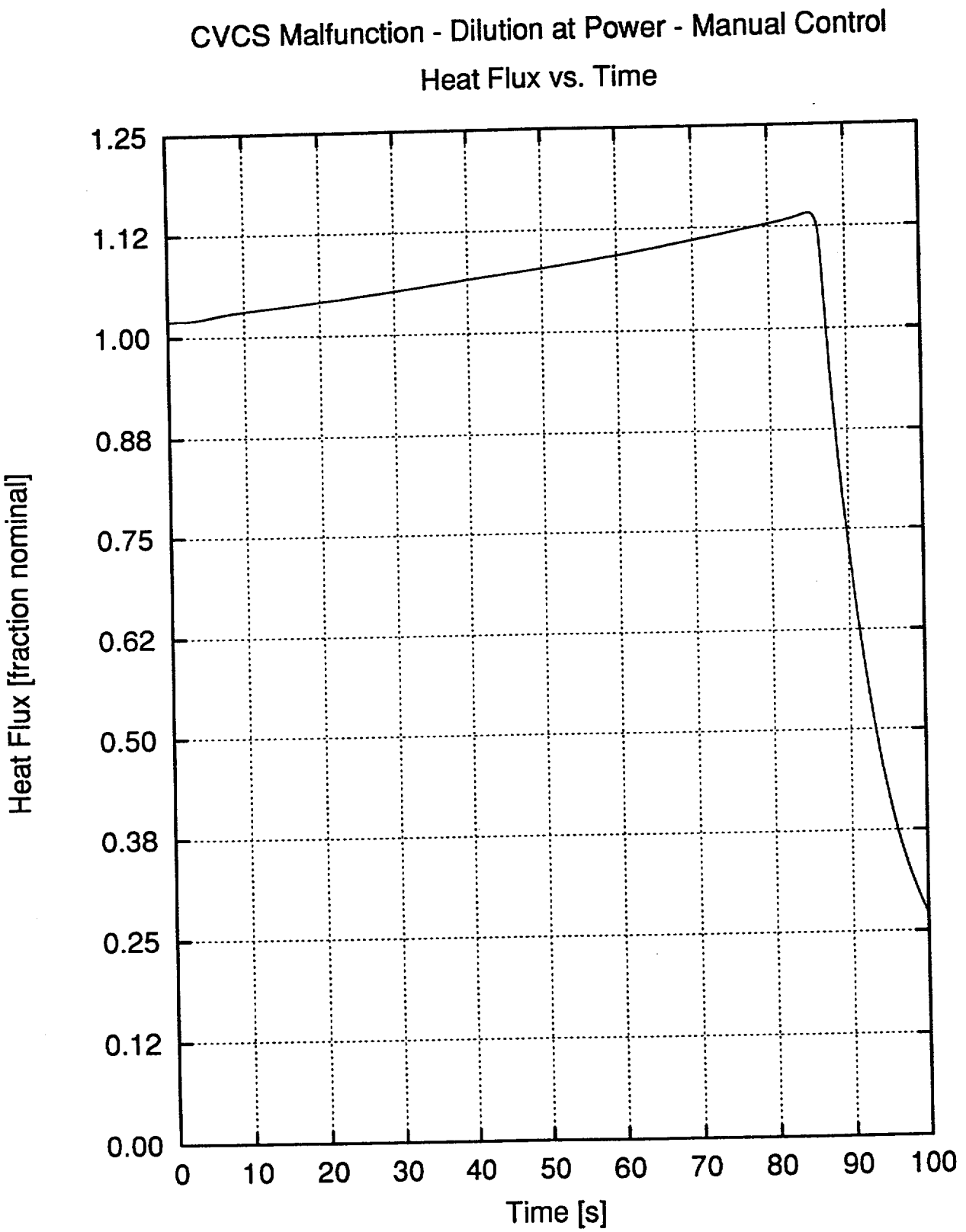

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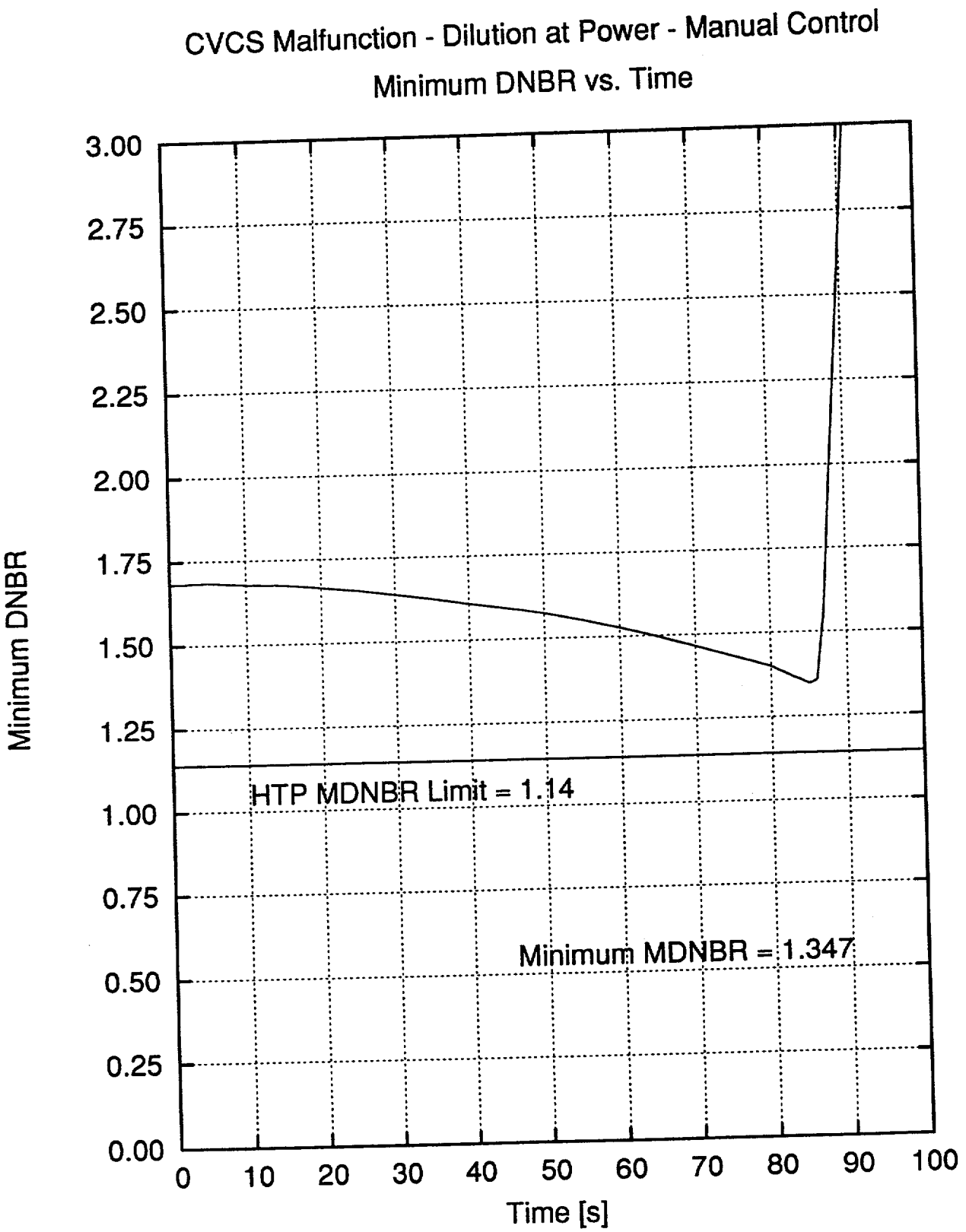

Figure 14.1.4-5

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STARTUP OF INACTIVE RX COOLANT LOOP

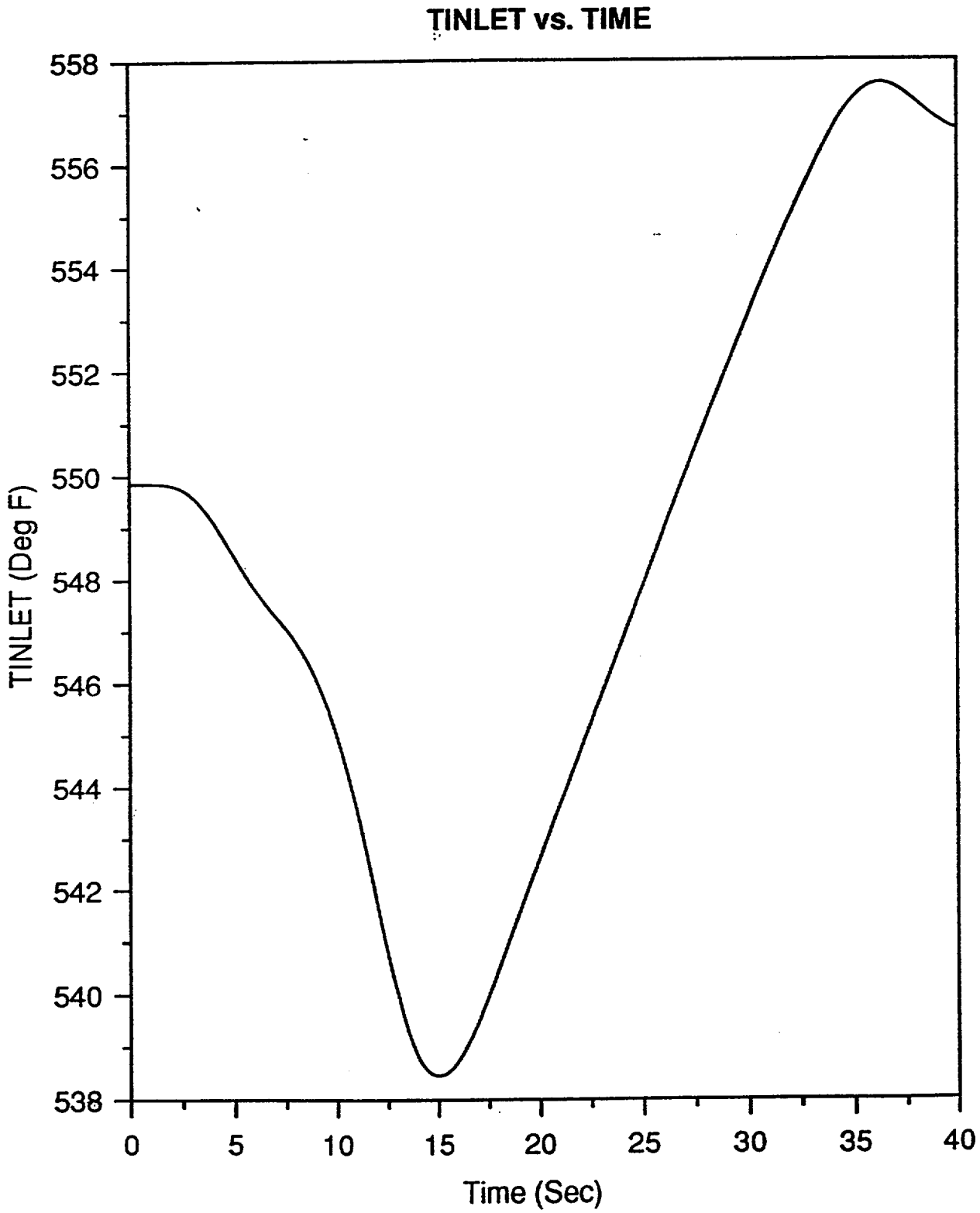

FIGURE 14.1.5-1

STARTUP OF INACTIVE RX COOLANT LOOP

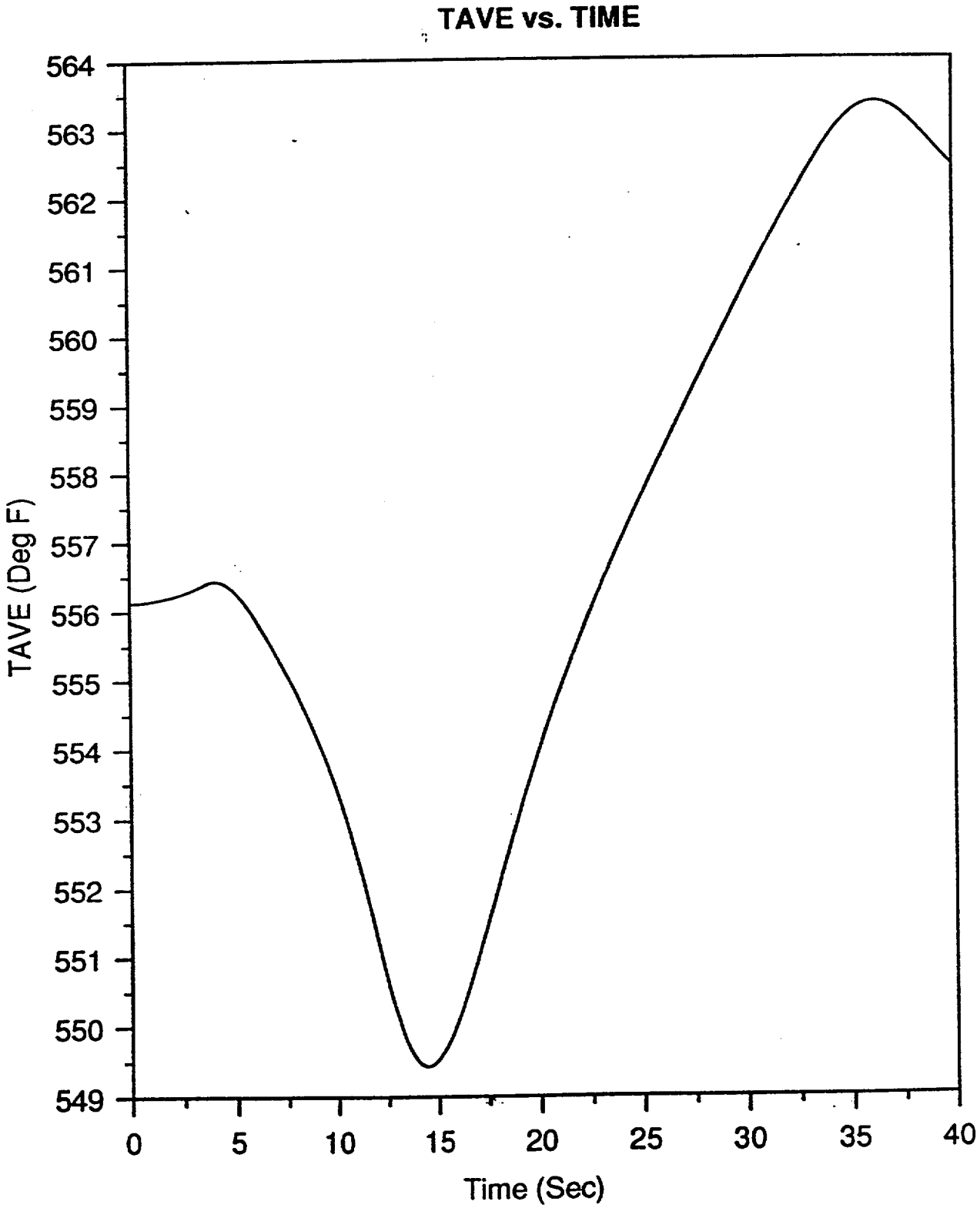

FIGURE 14.1.5-2

STARTUP OF INACTIVE RX COOLANT LOOP

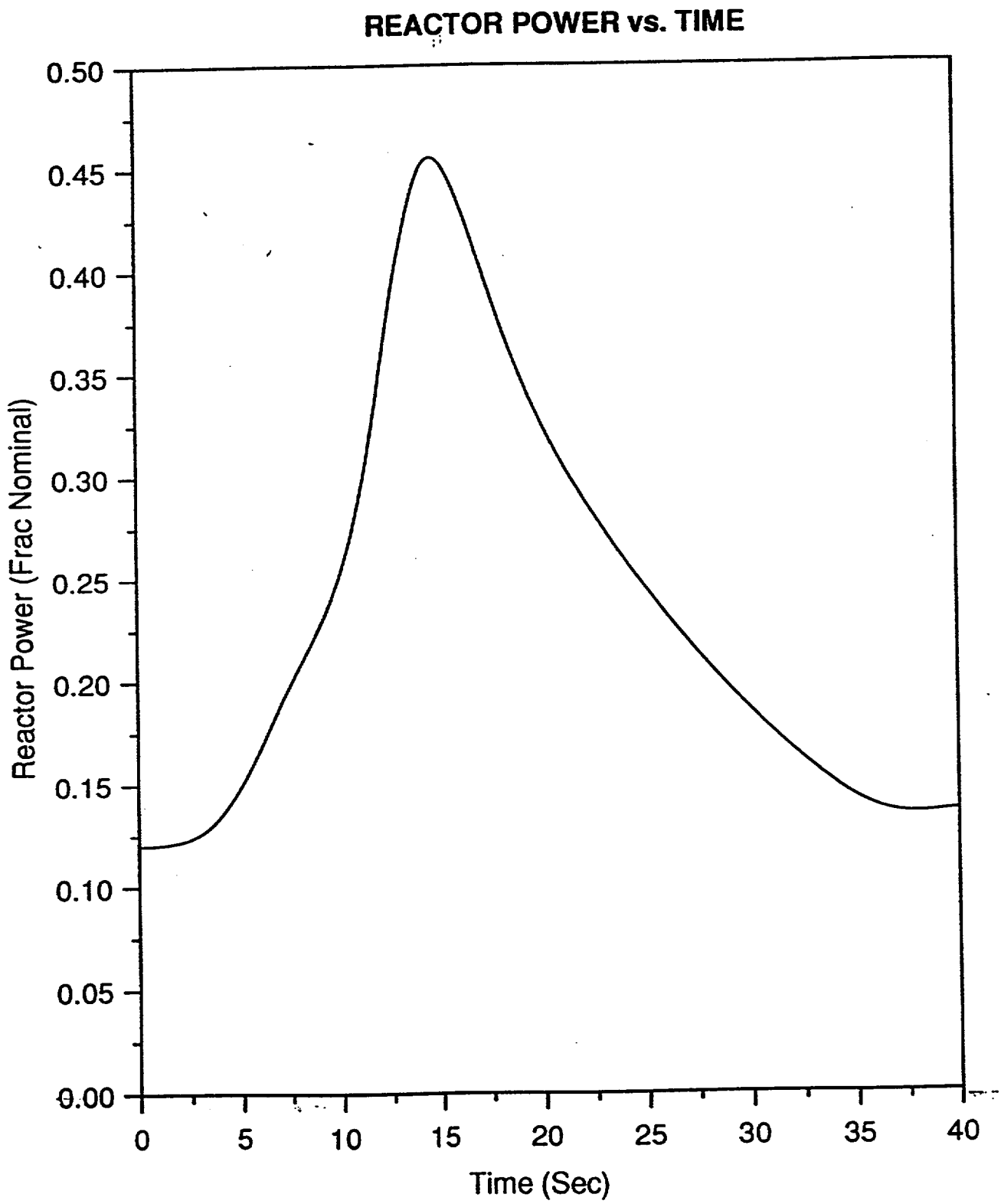

FIGURE 14.1.5-3

STARTUP OF INACTIVE RX COOLANT LOOP

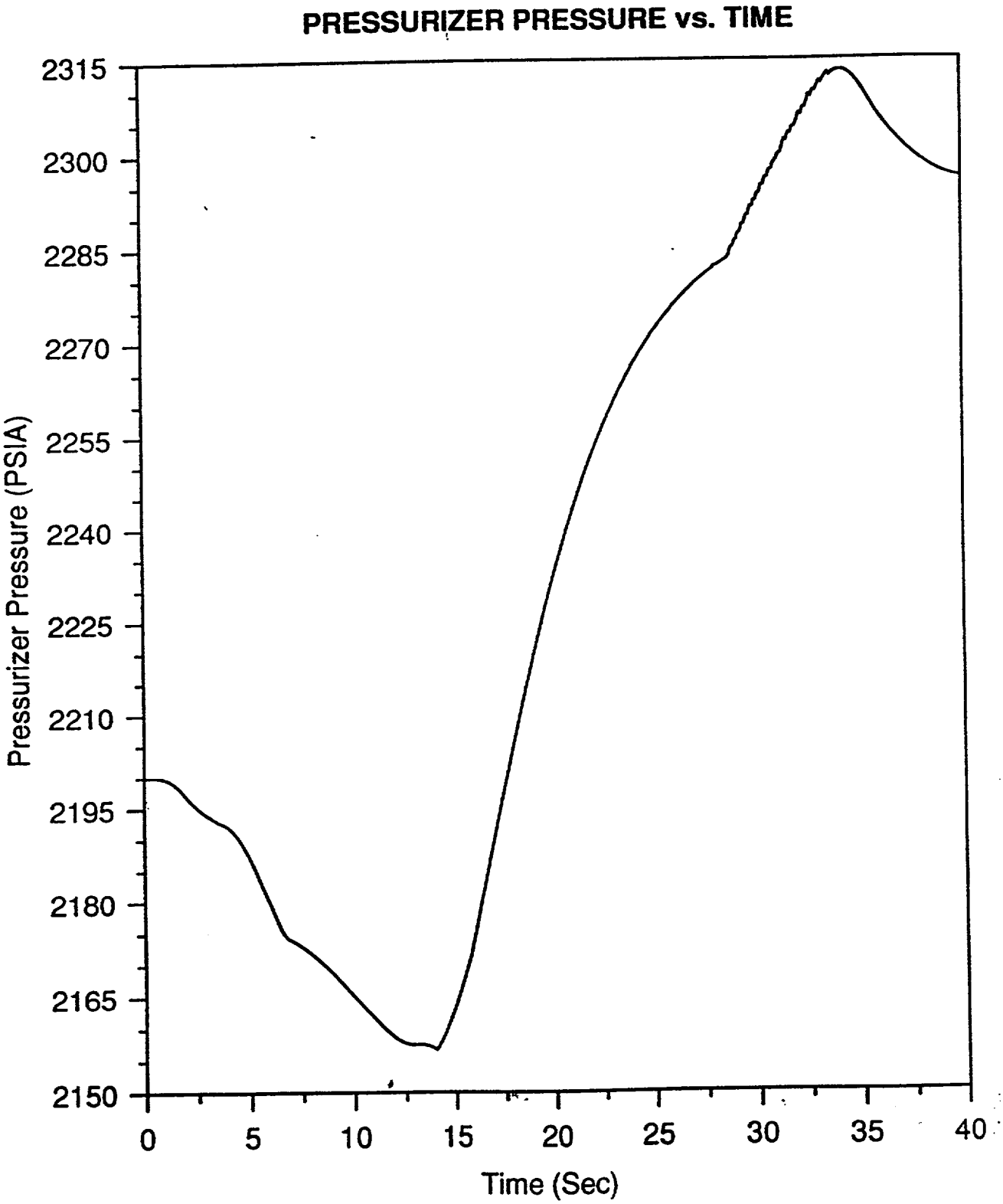

FIGURE 14.1.5-4

STARTUP OF INACTIVE RX COOLANT LOOP

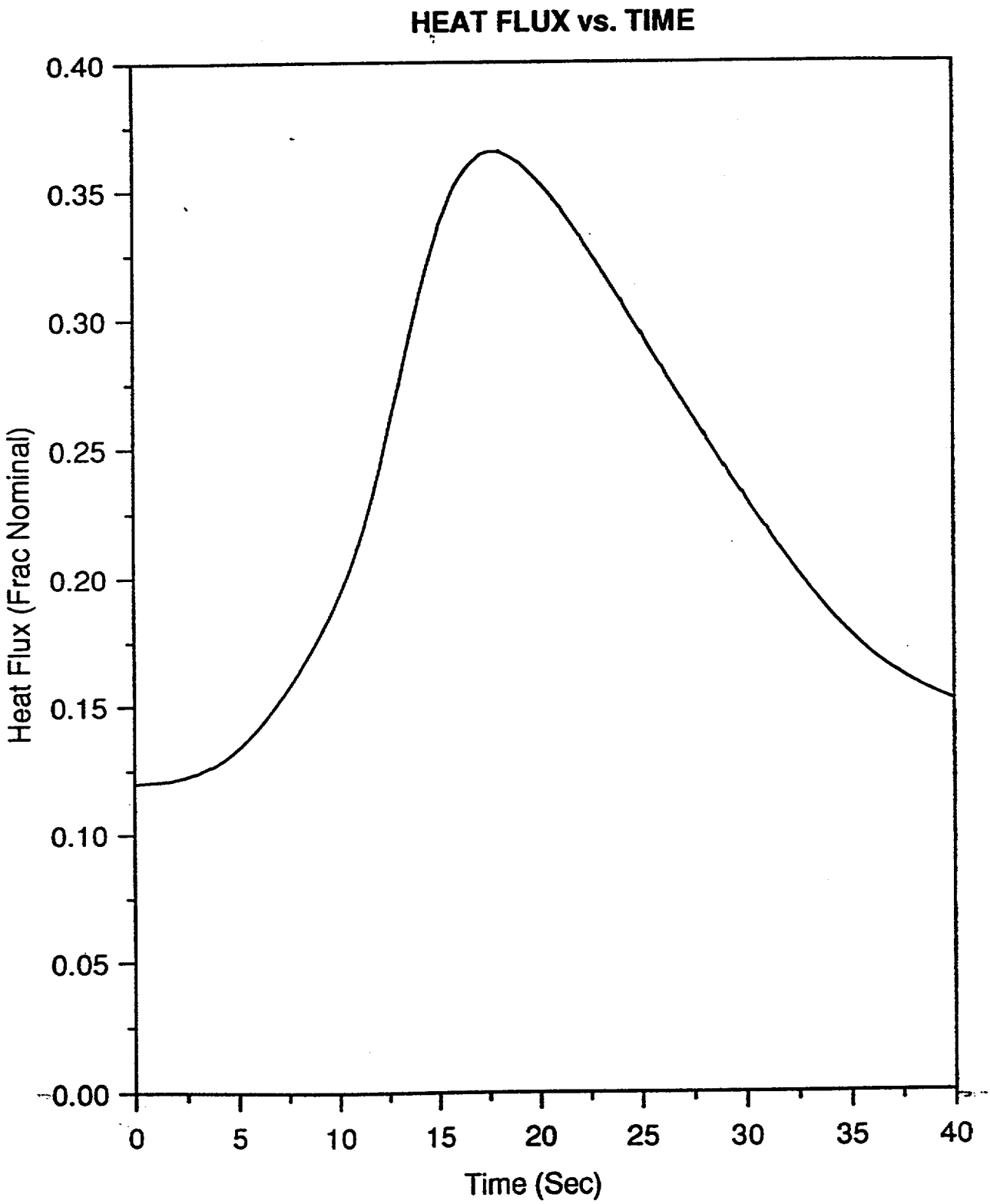

FIGURE 14.1.5-5

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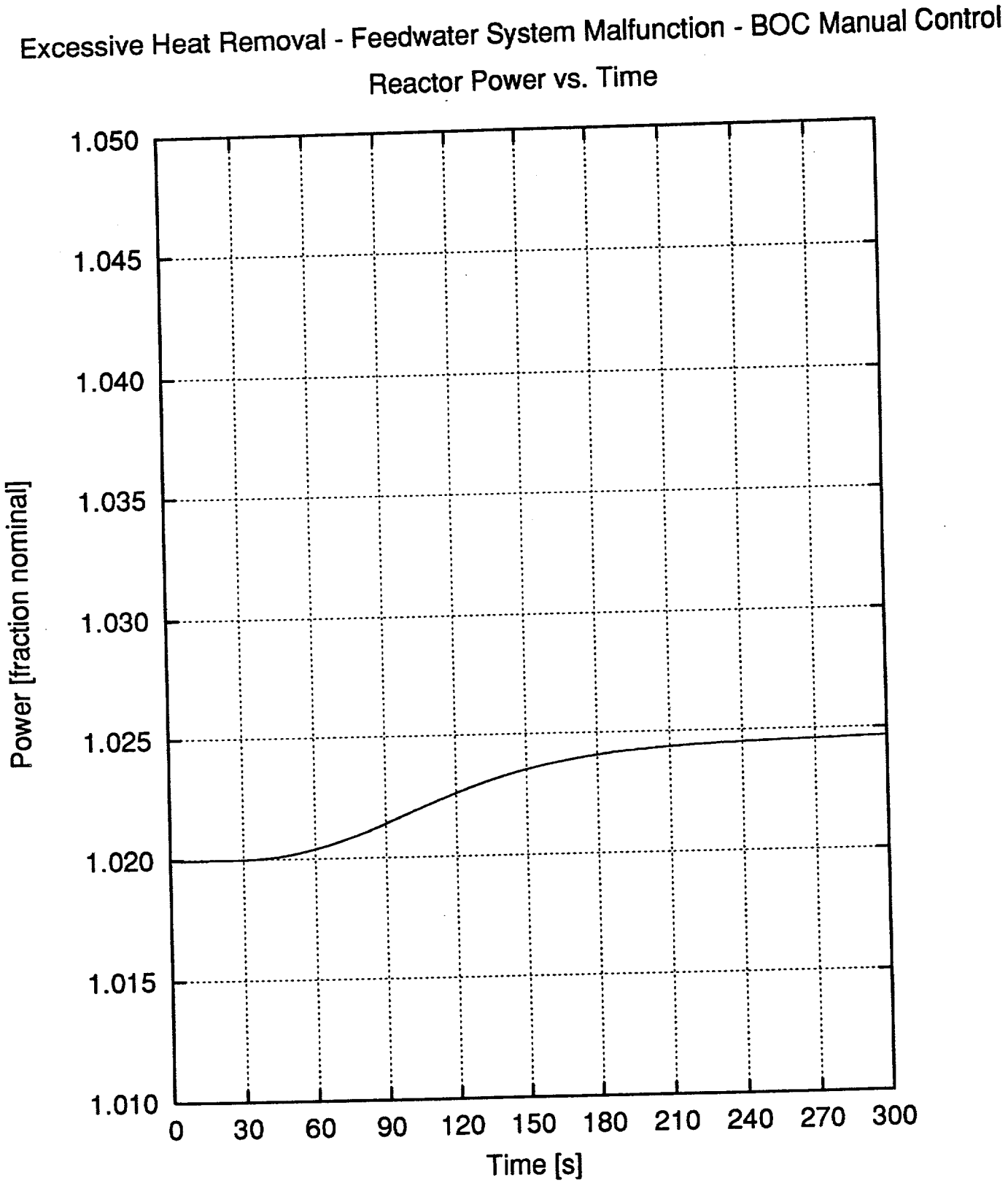

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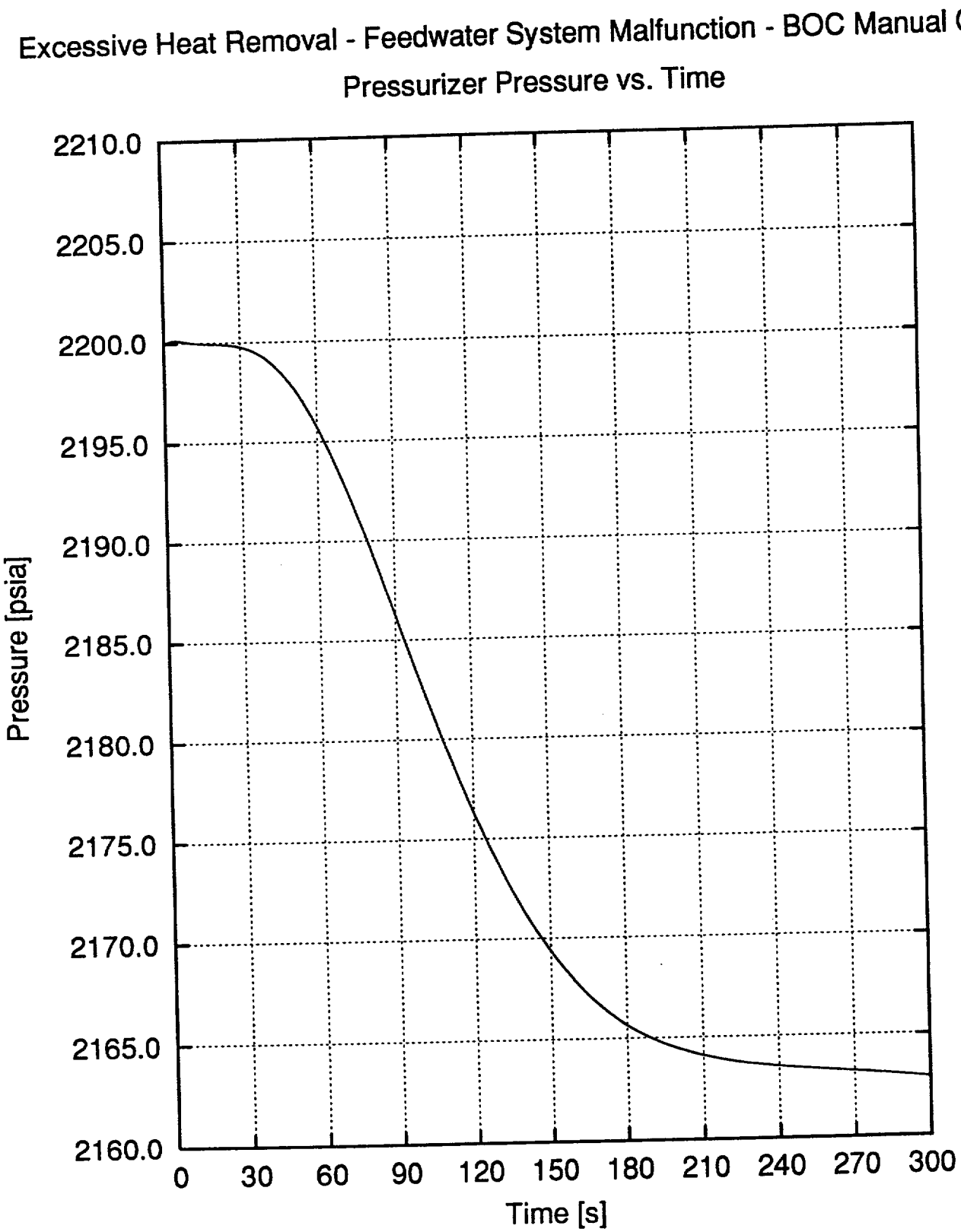

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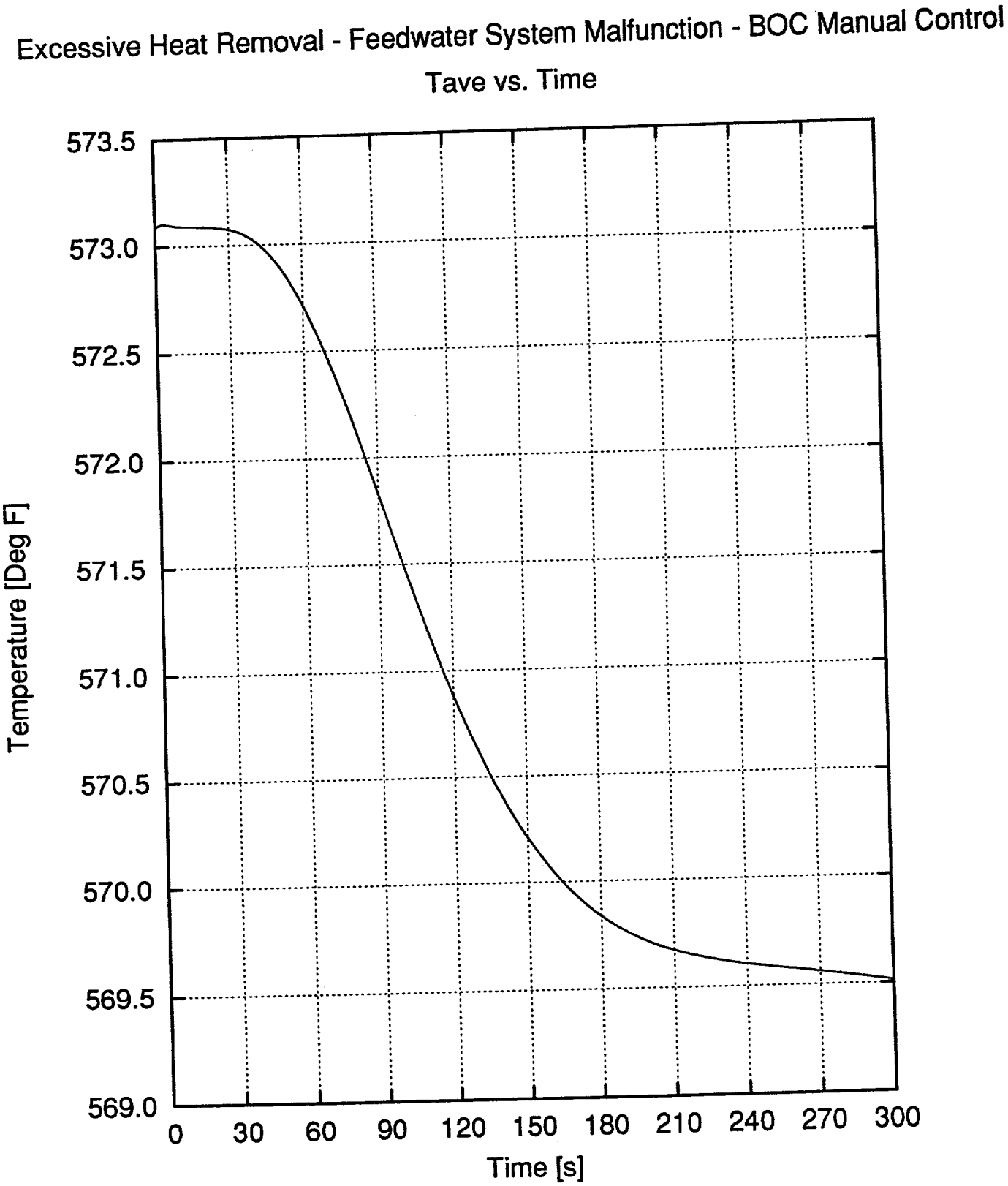

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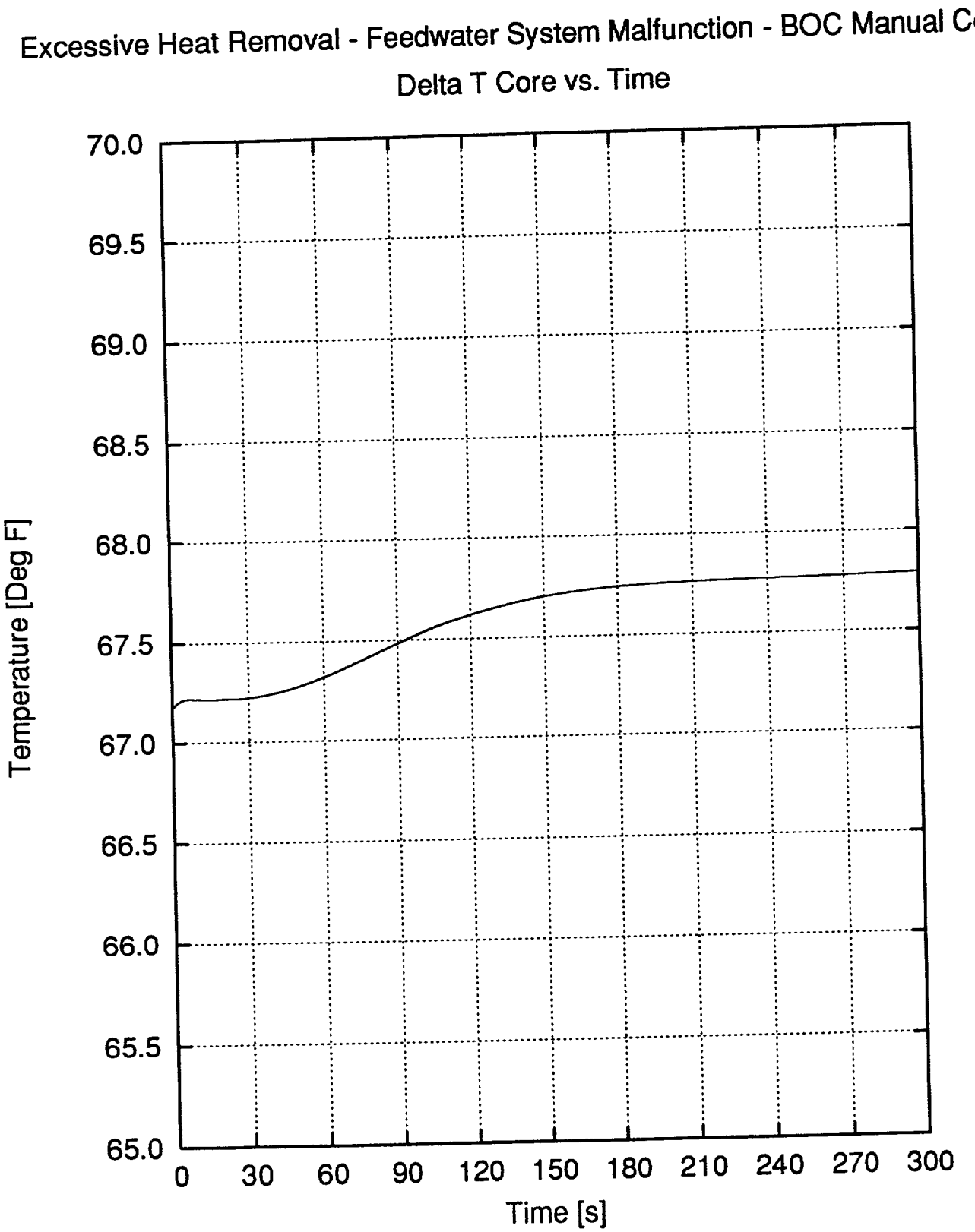

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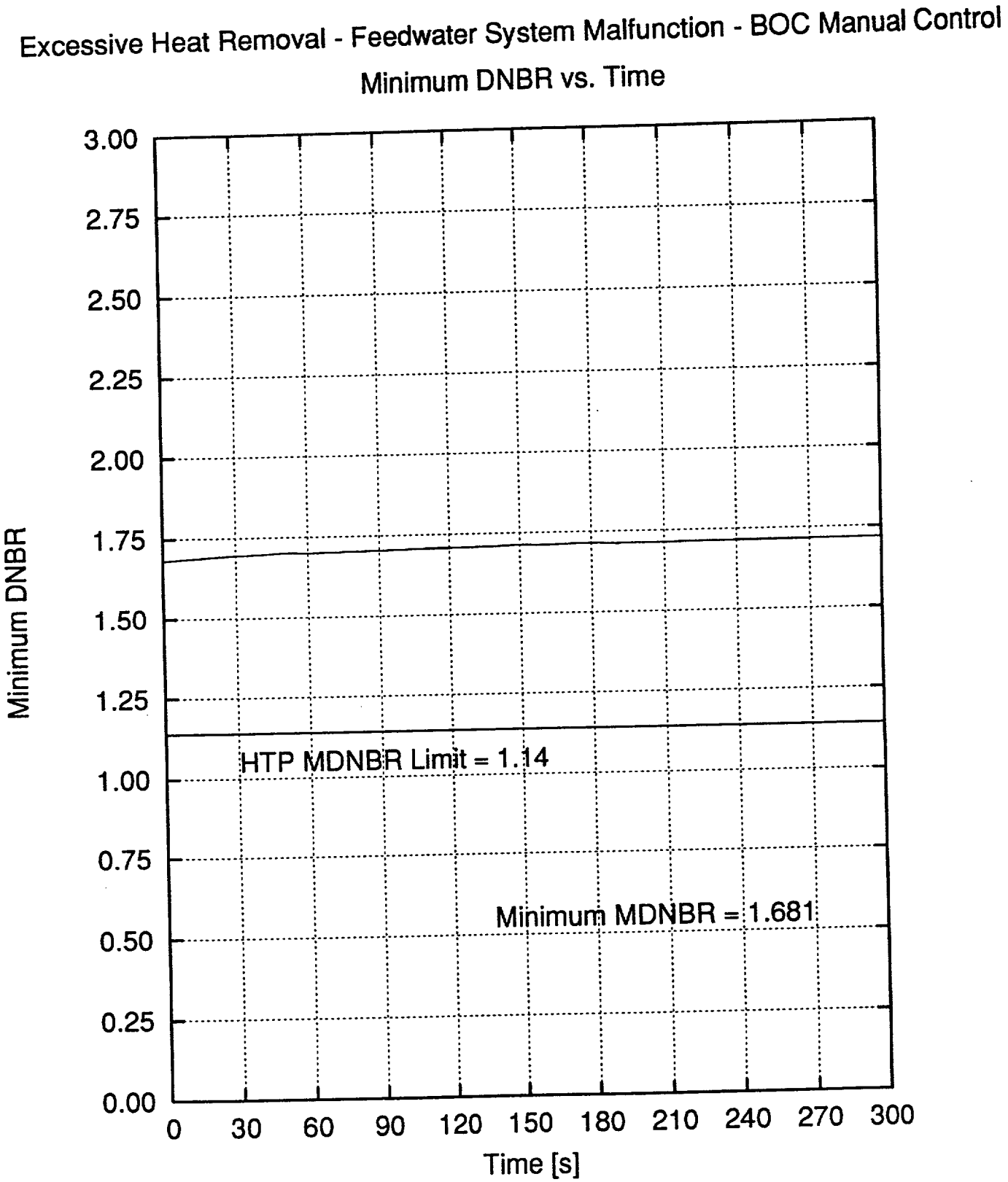

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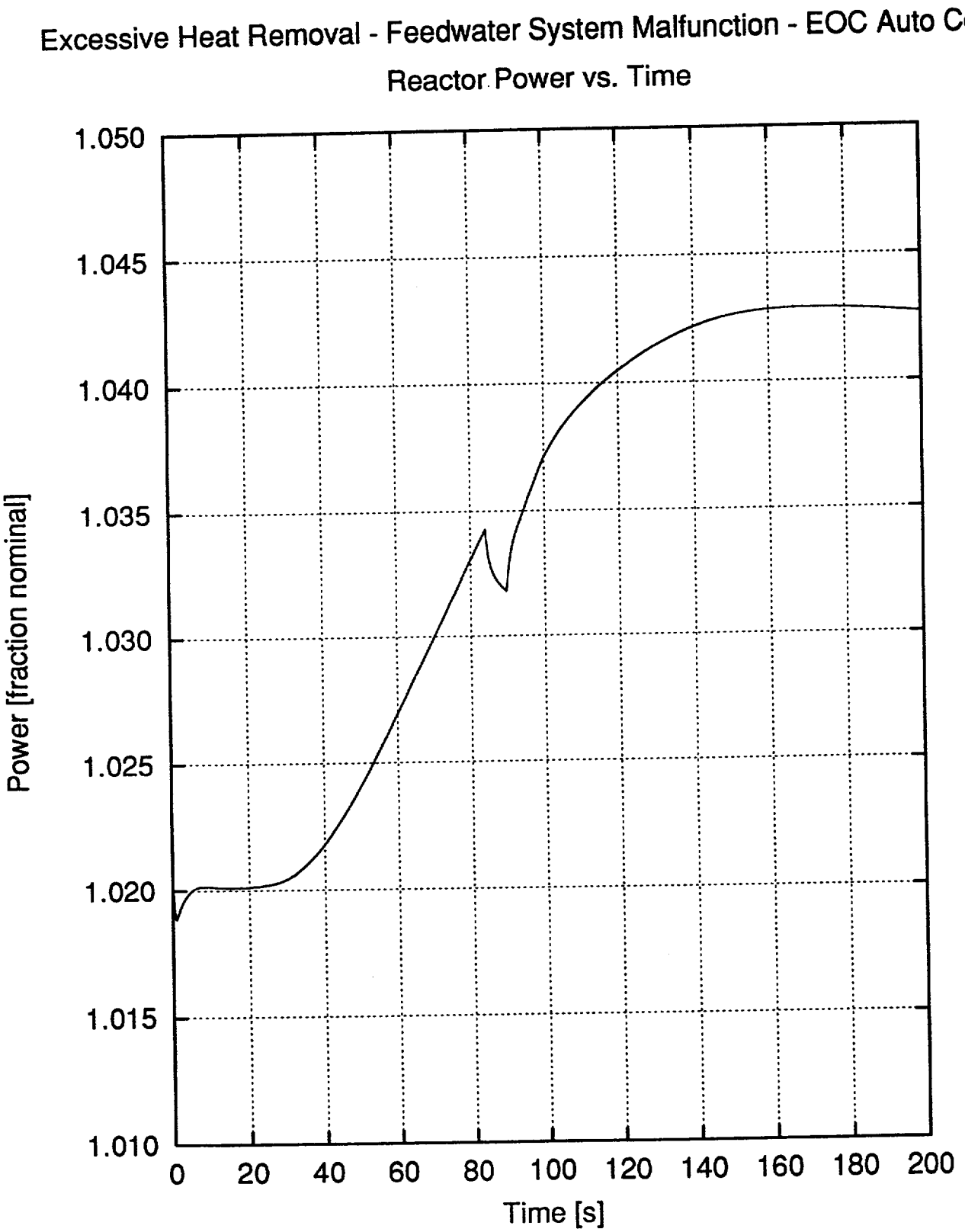

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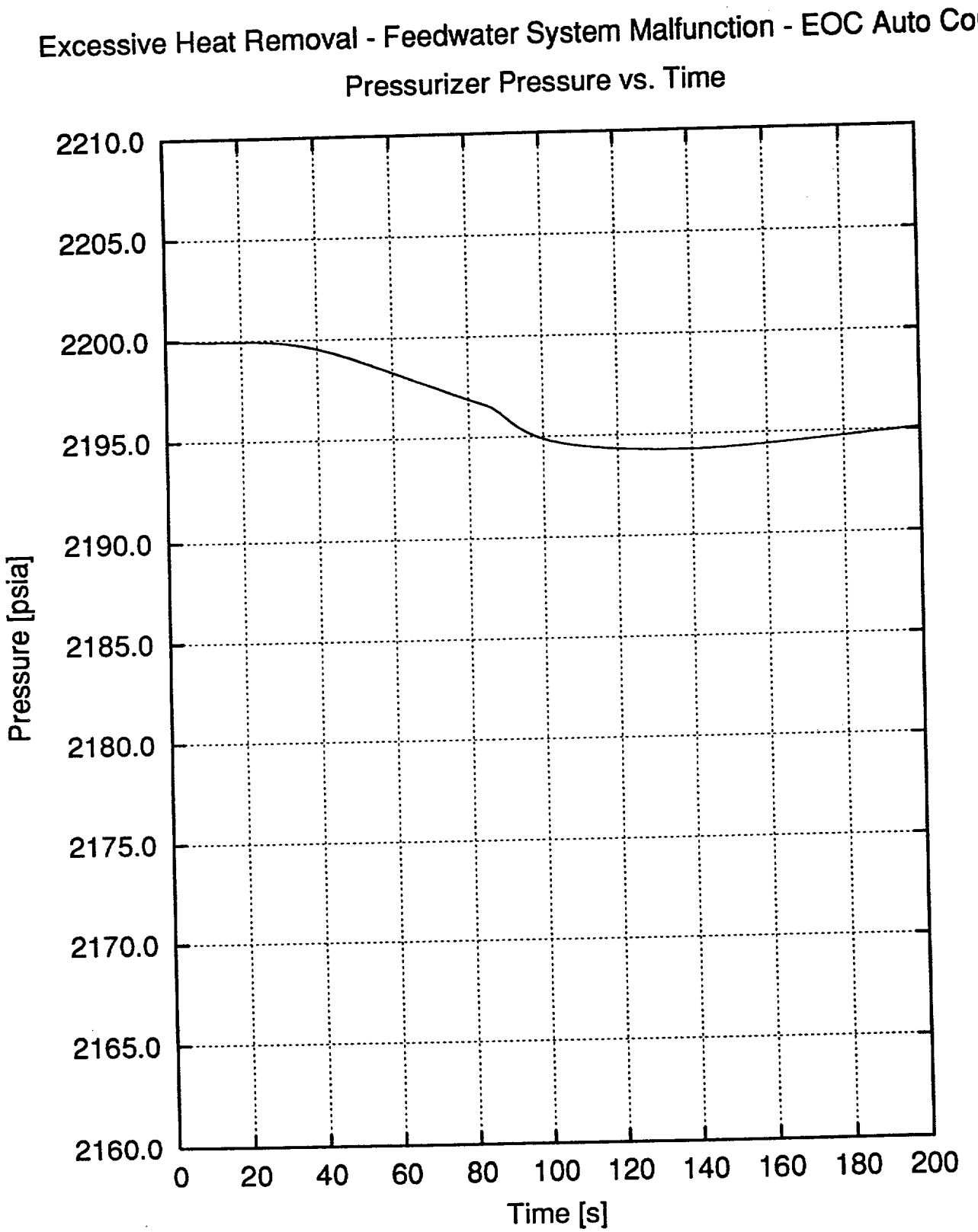

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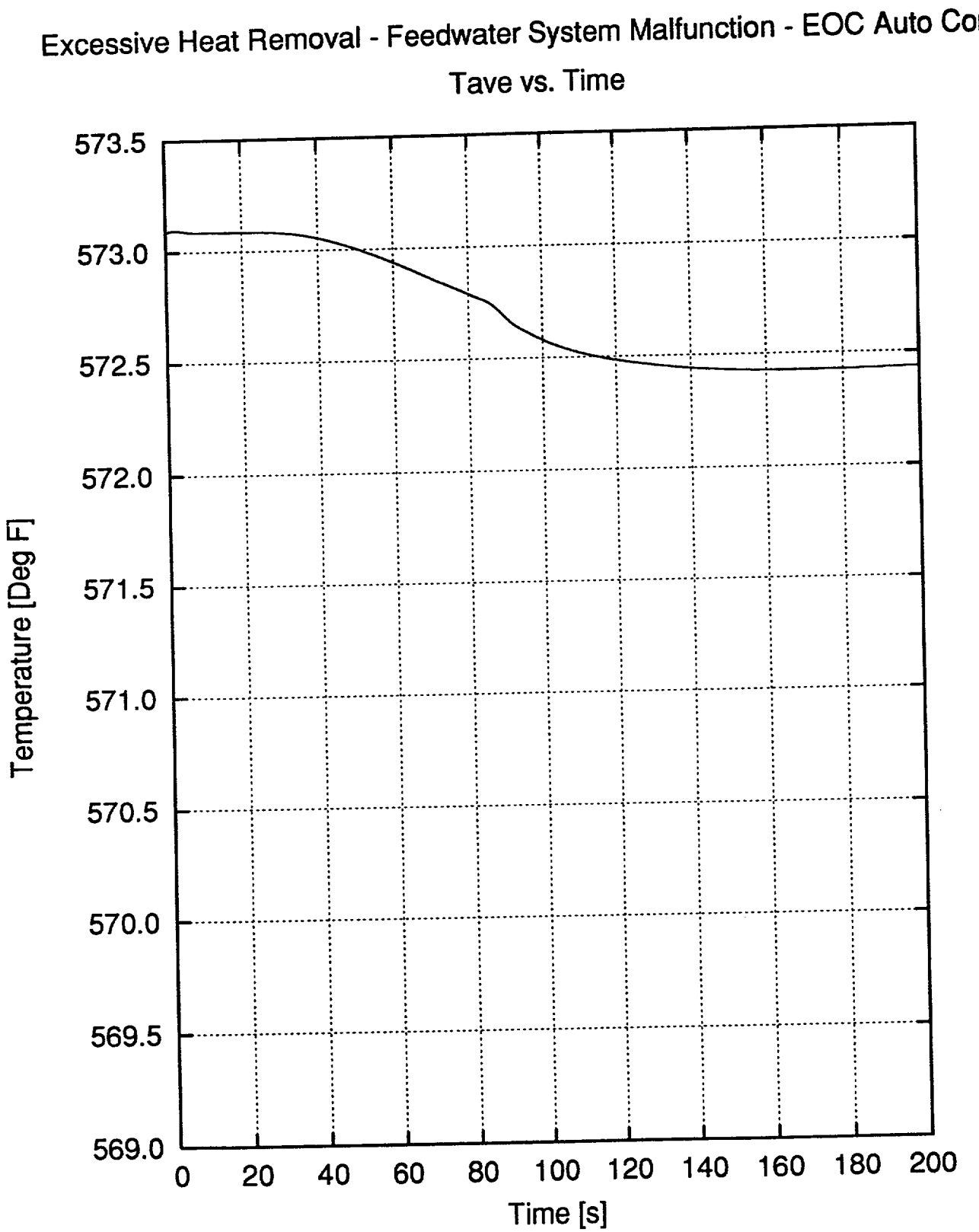

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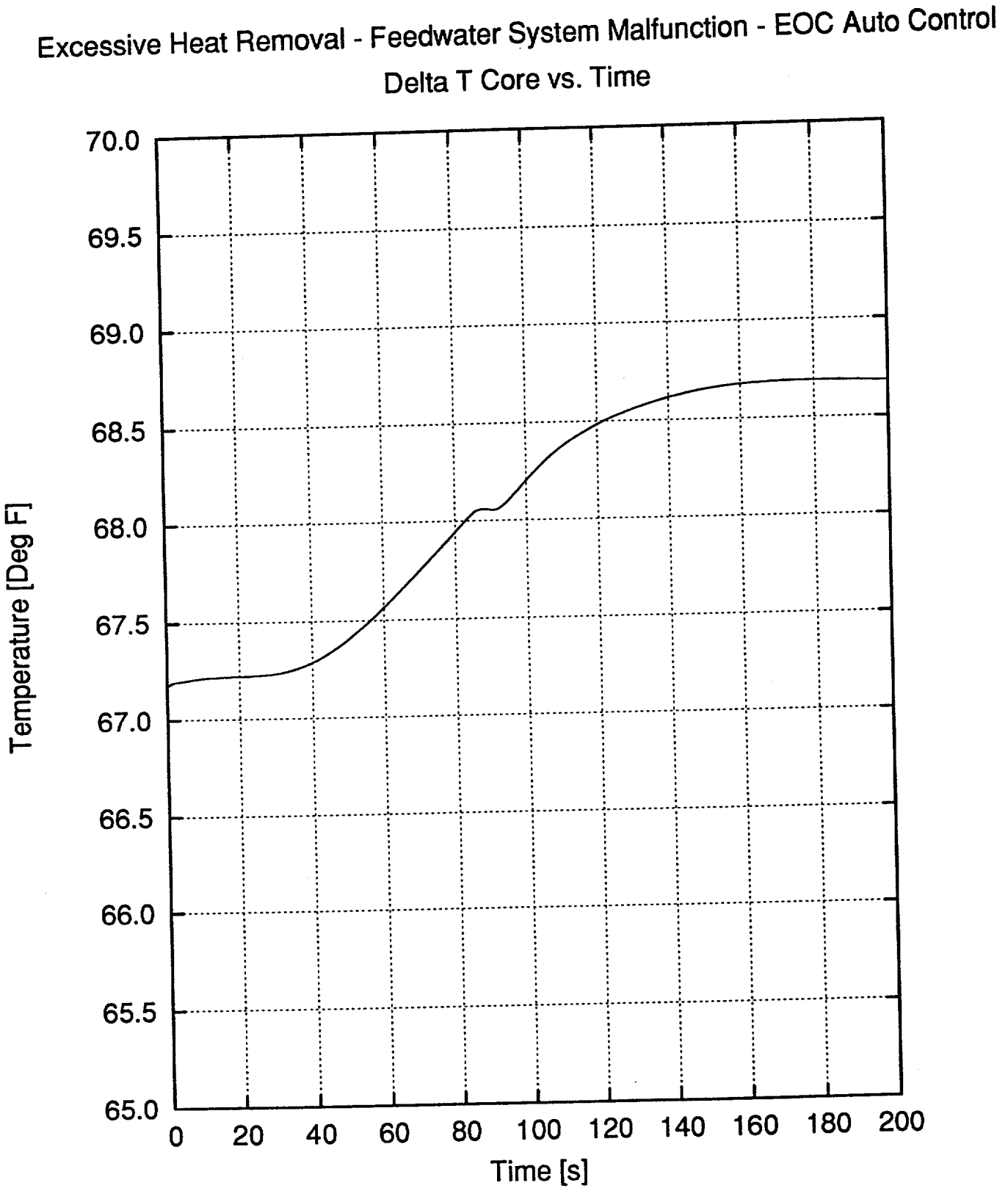
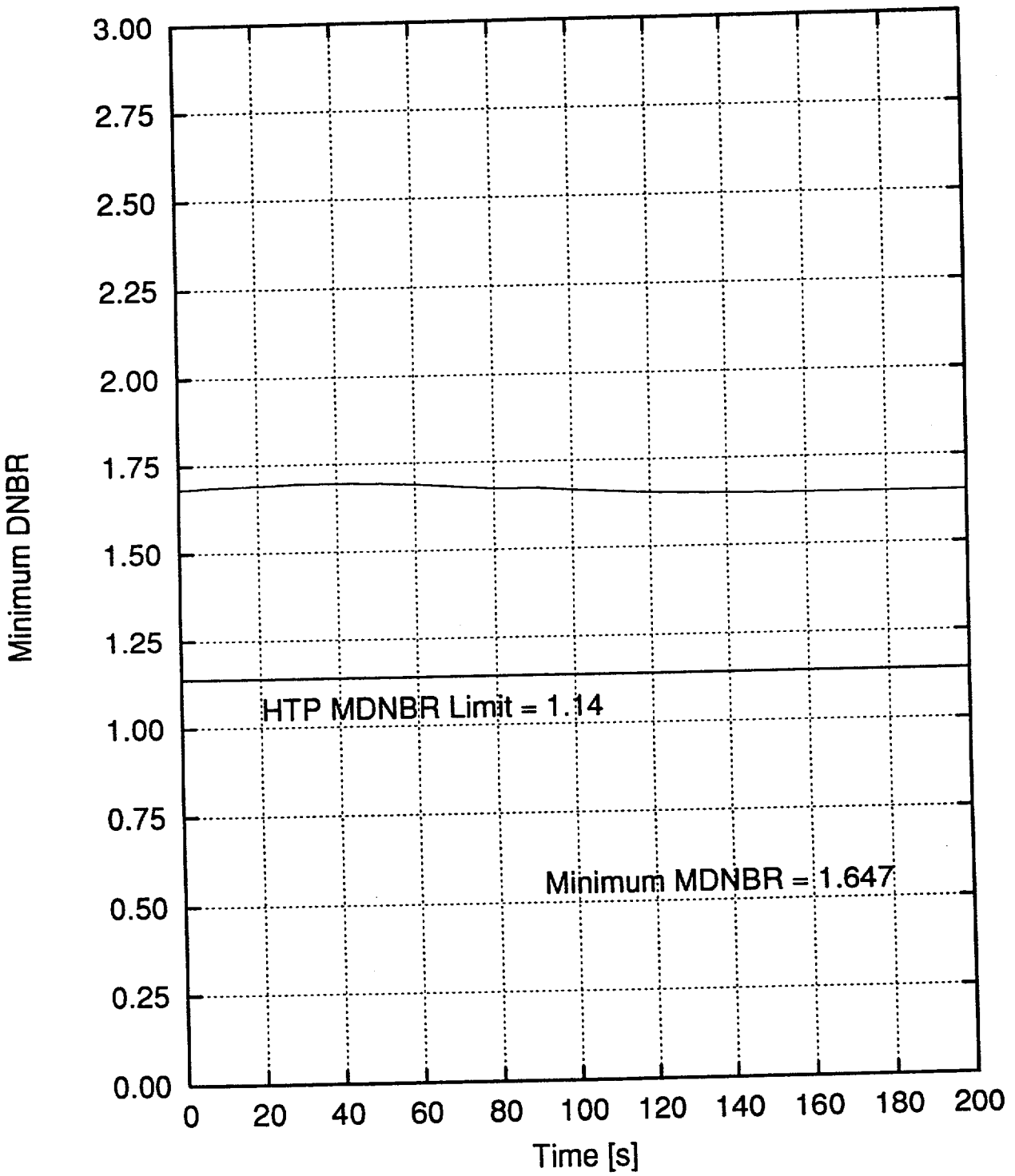


Figure 14.1.6-9

Excessive Heat Removal - Feedwater System Malfunction - EOC Auto Control

Minimum DNBR vs. Time



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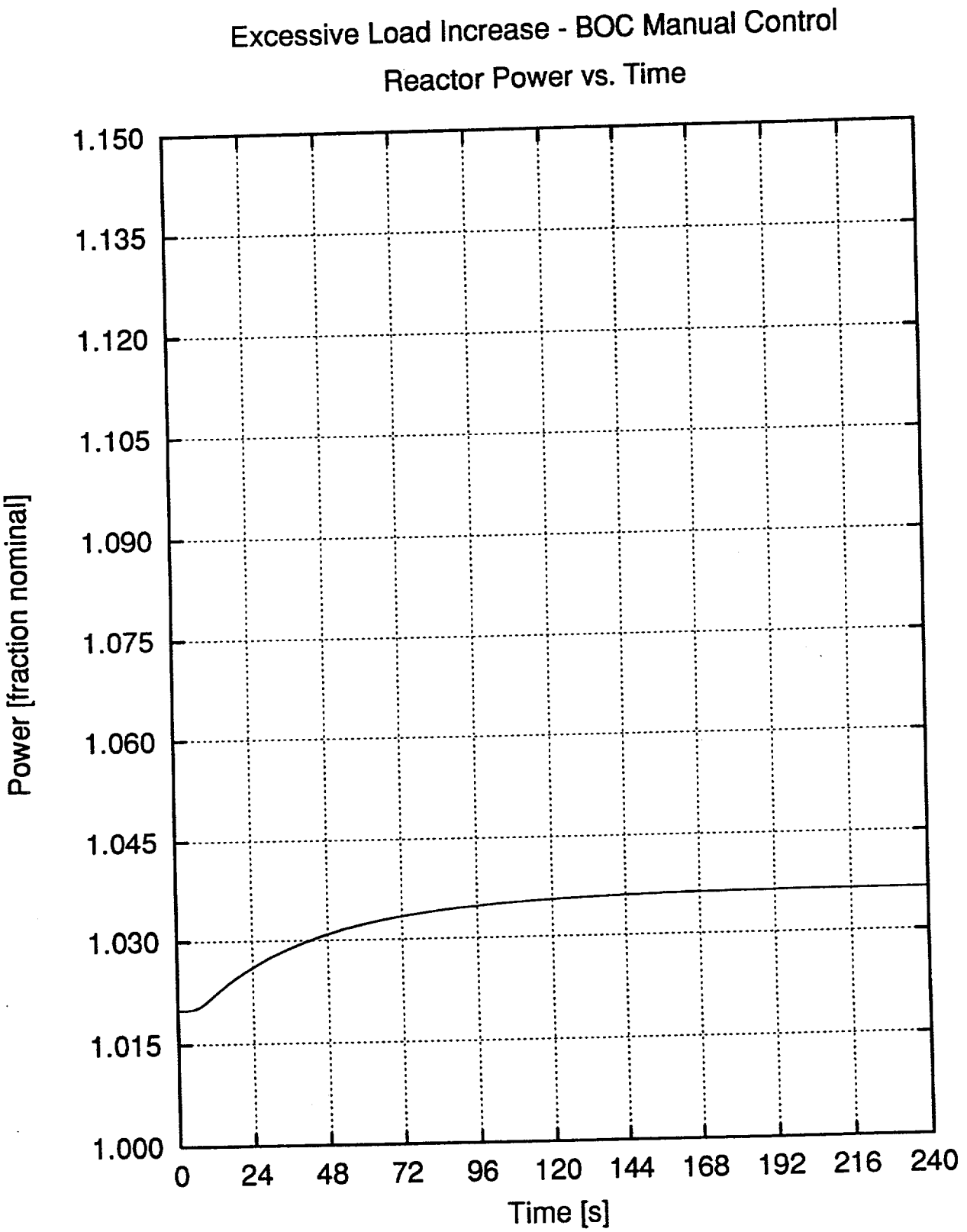

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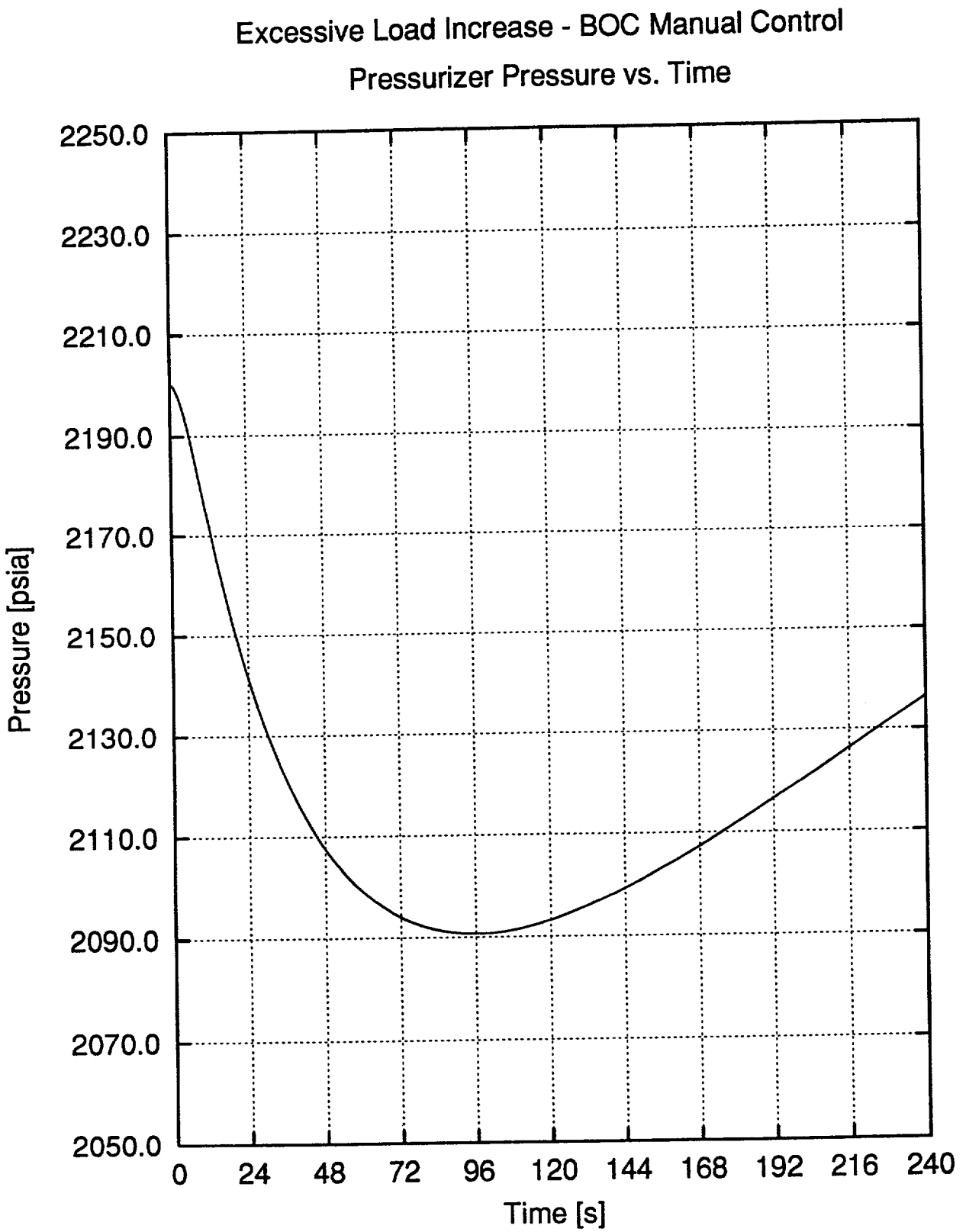

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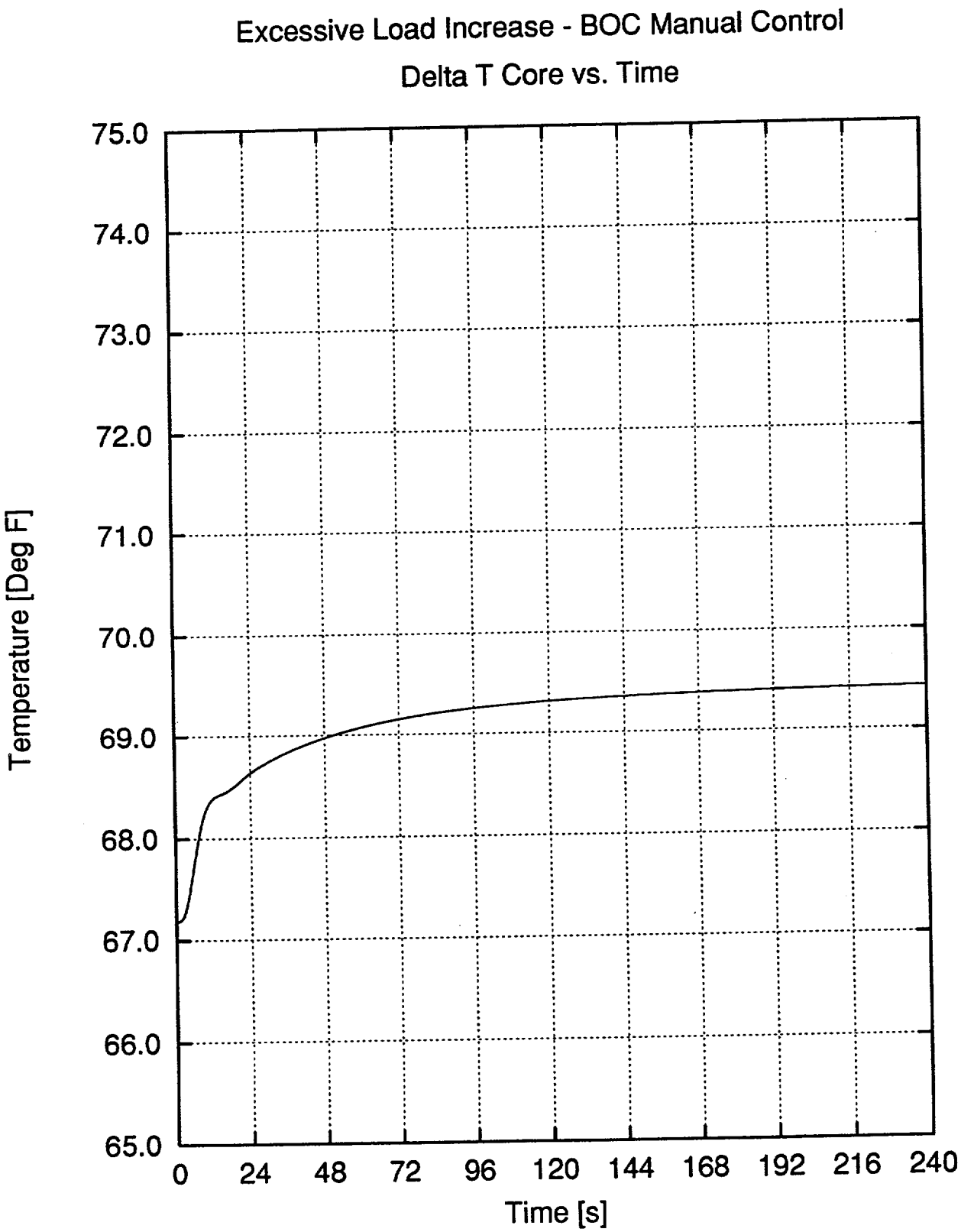

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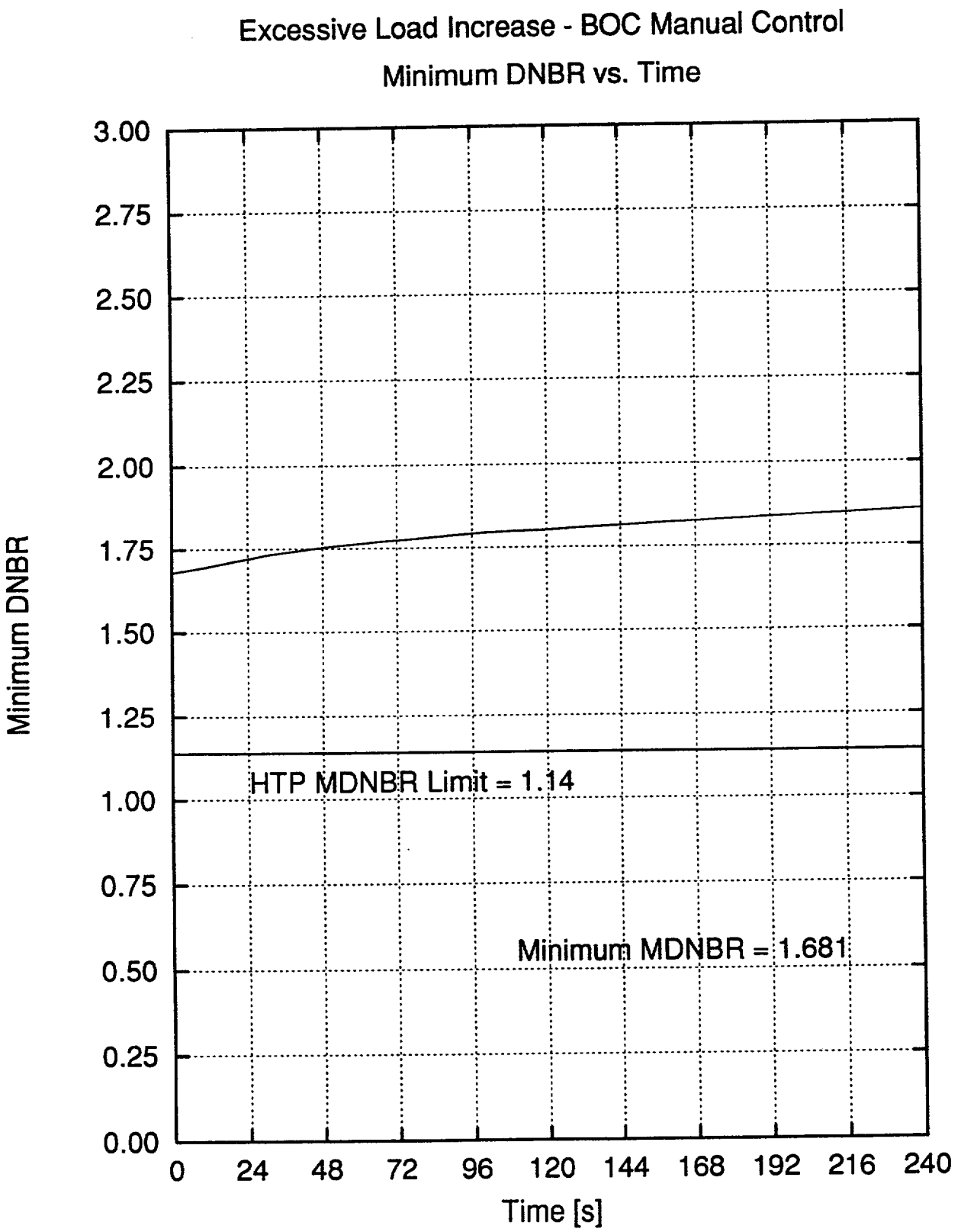

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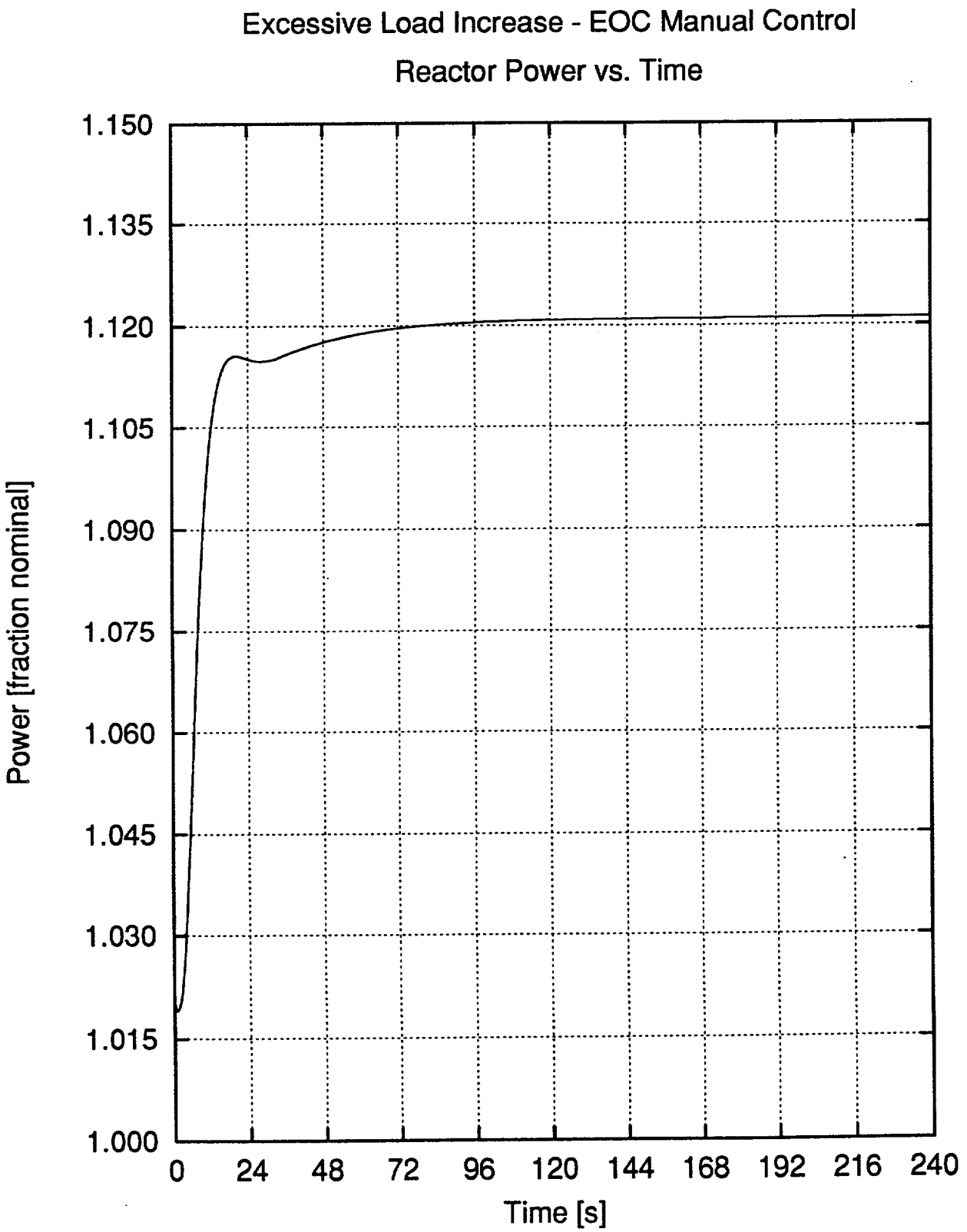

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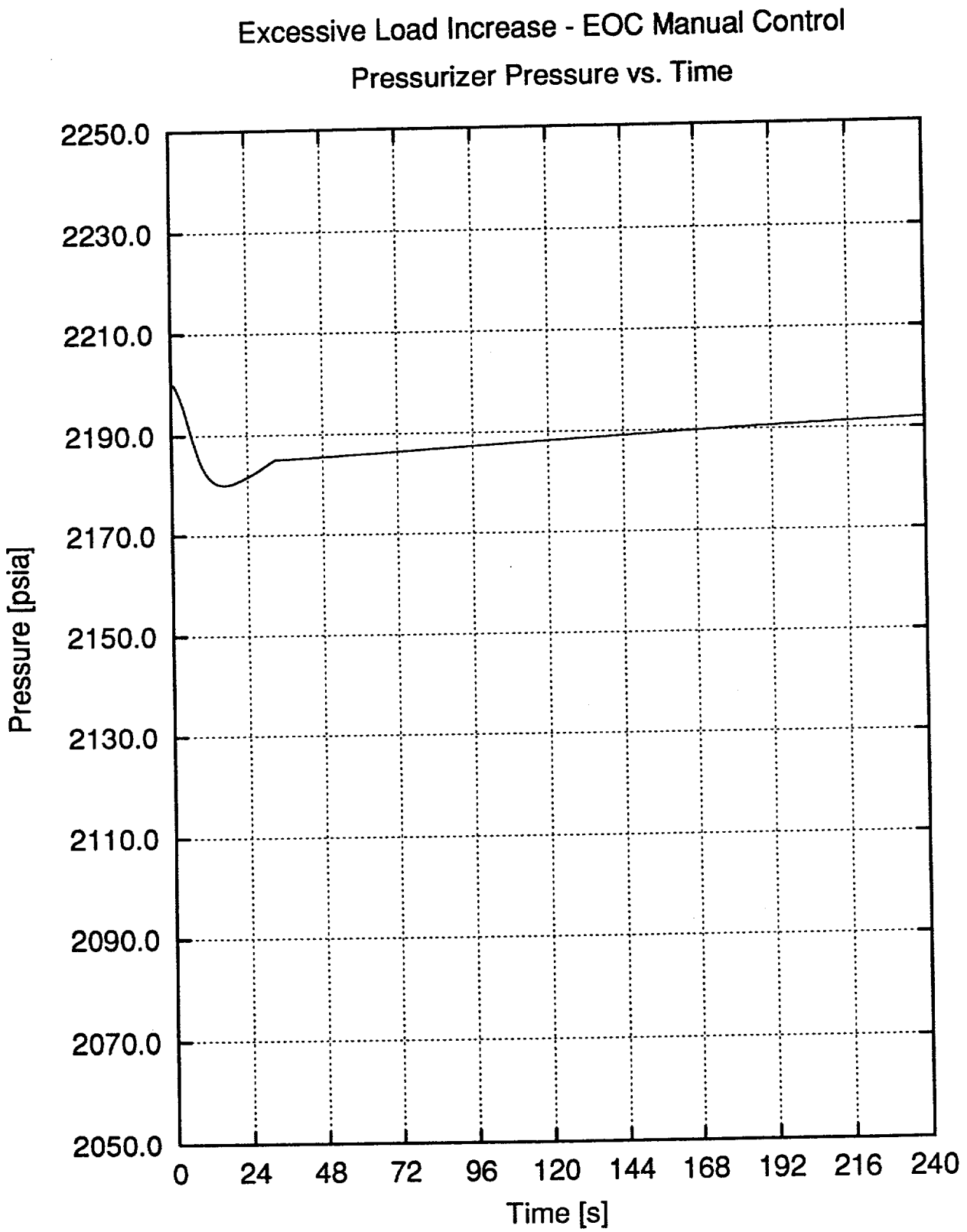

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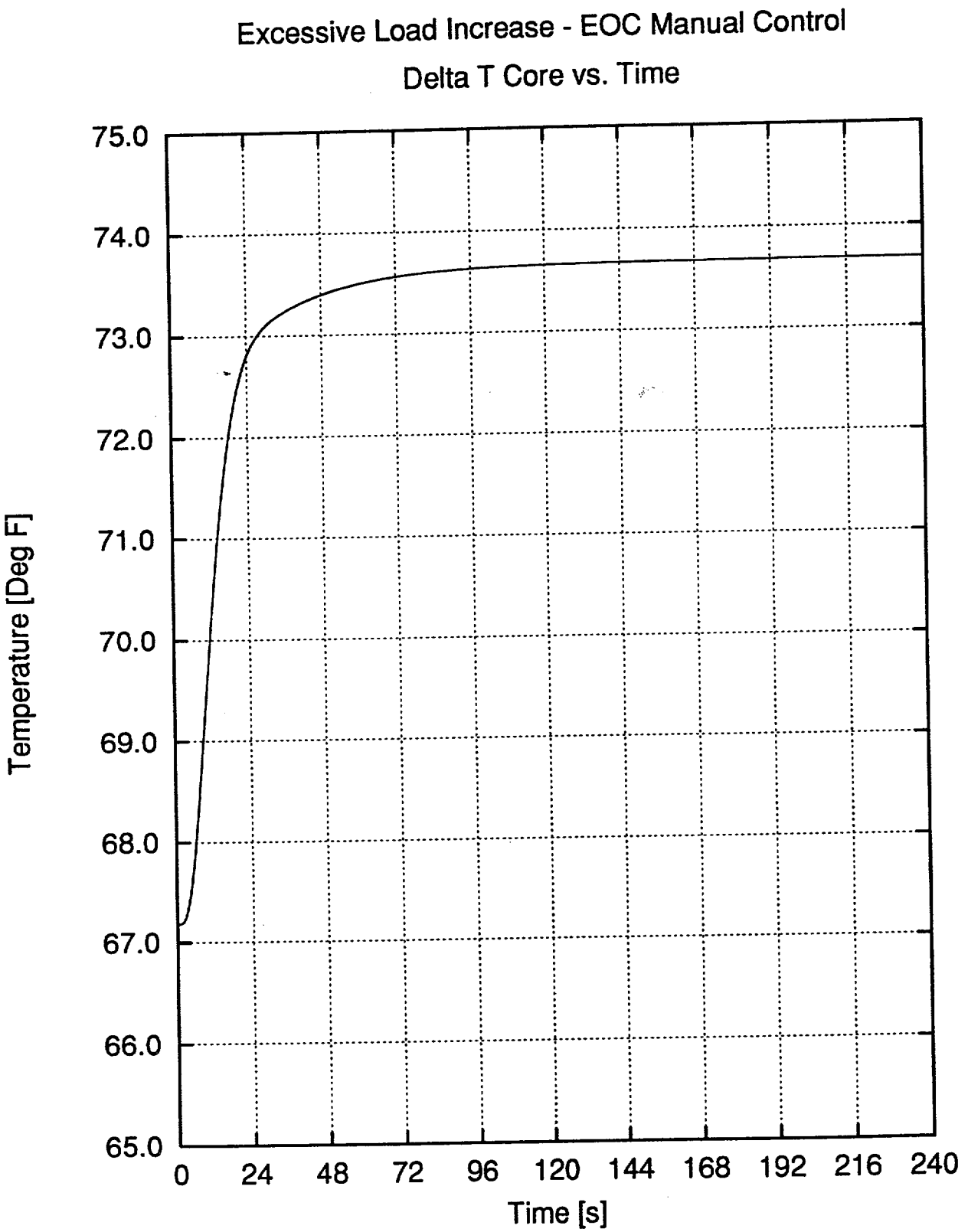

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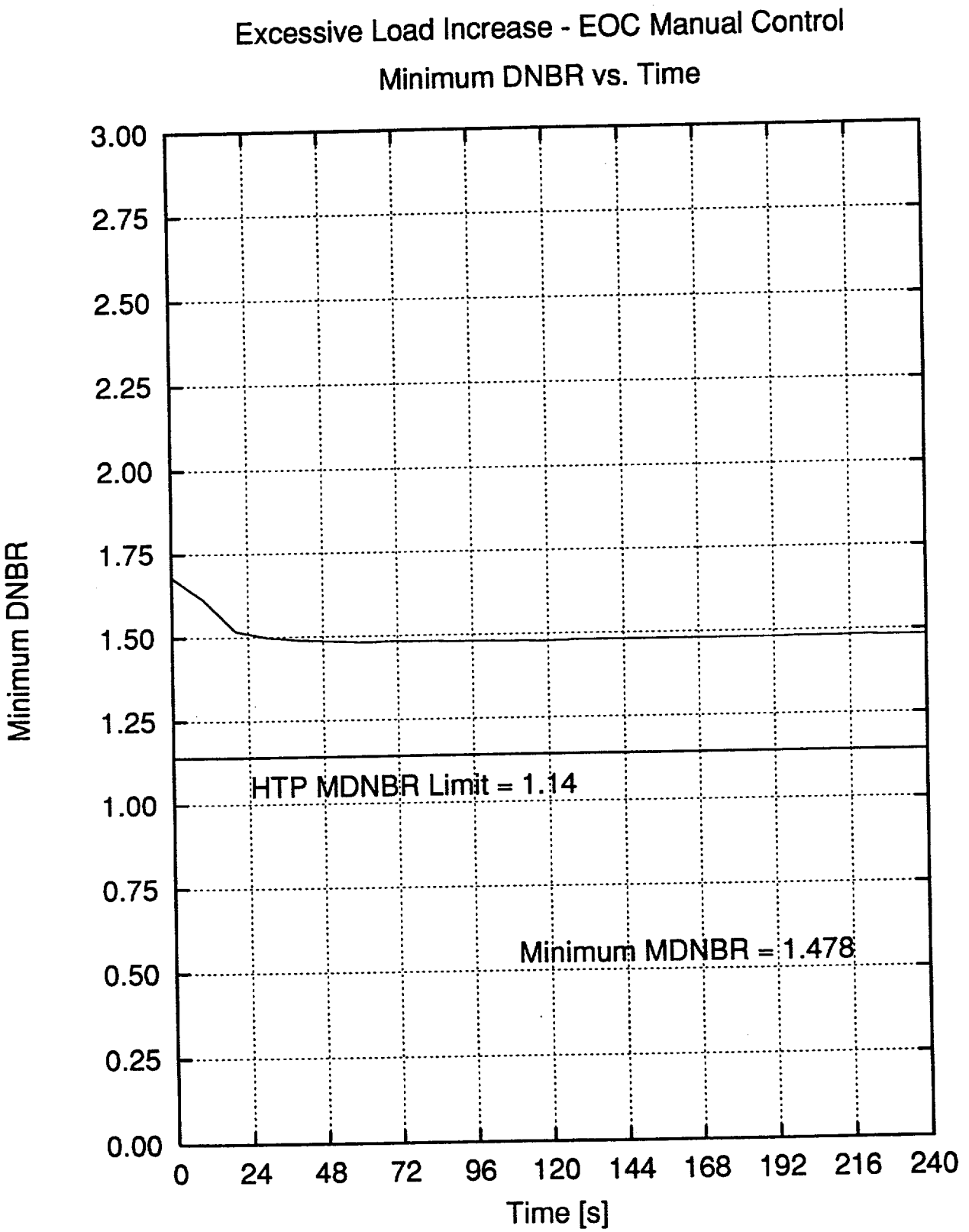

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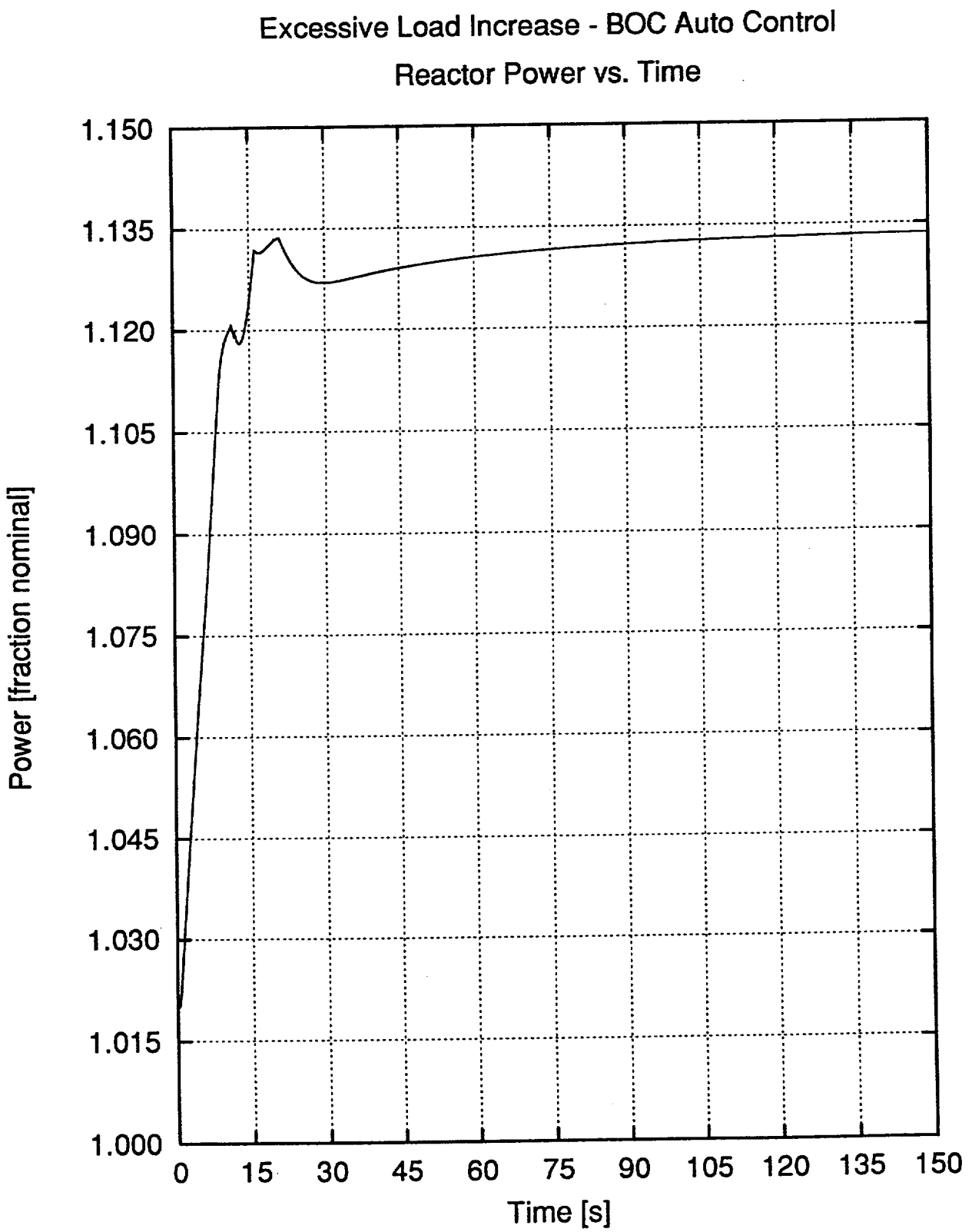

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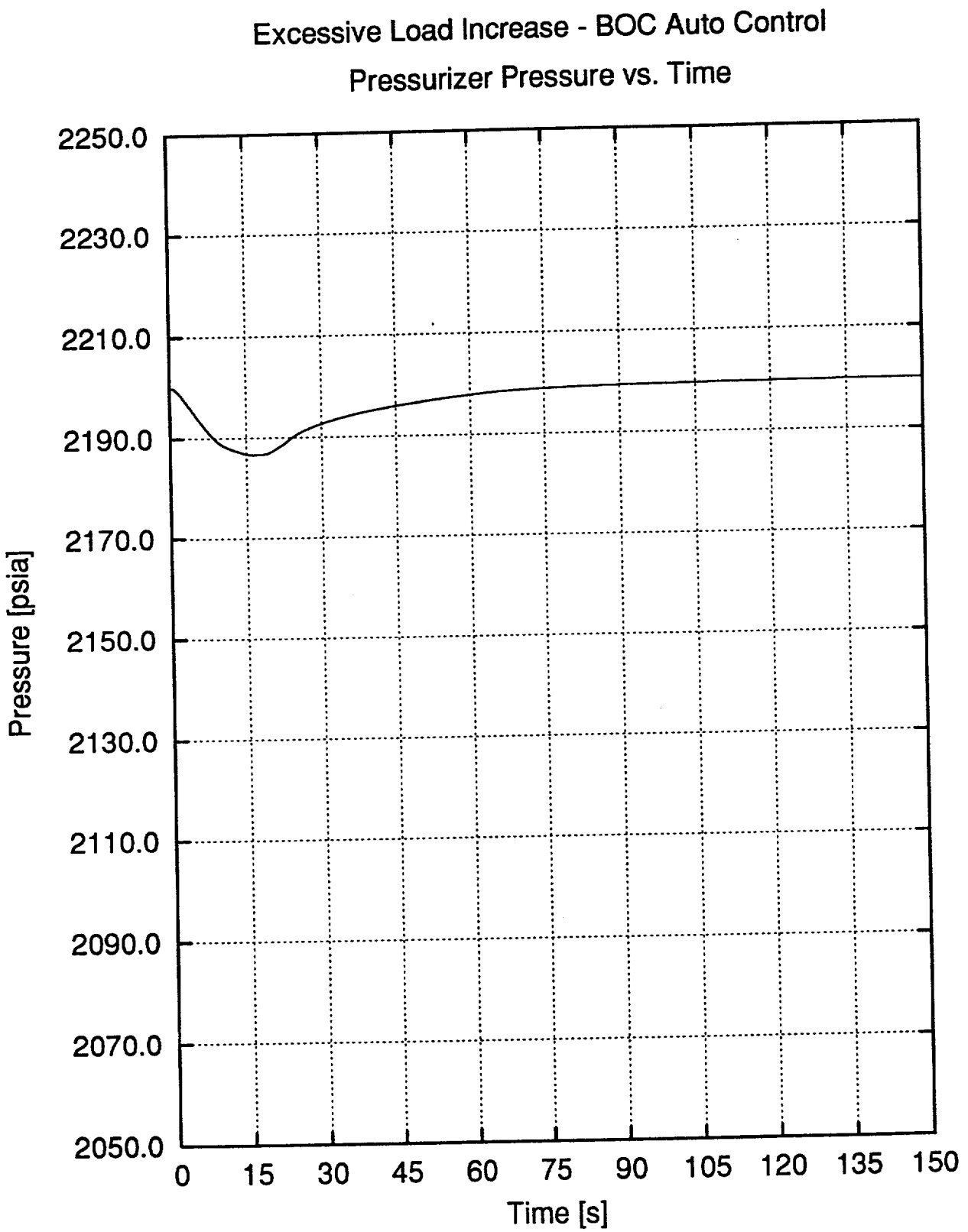

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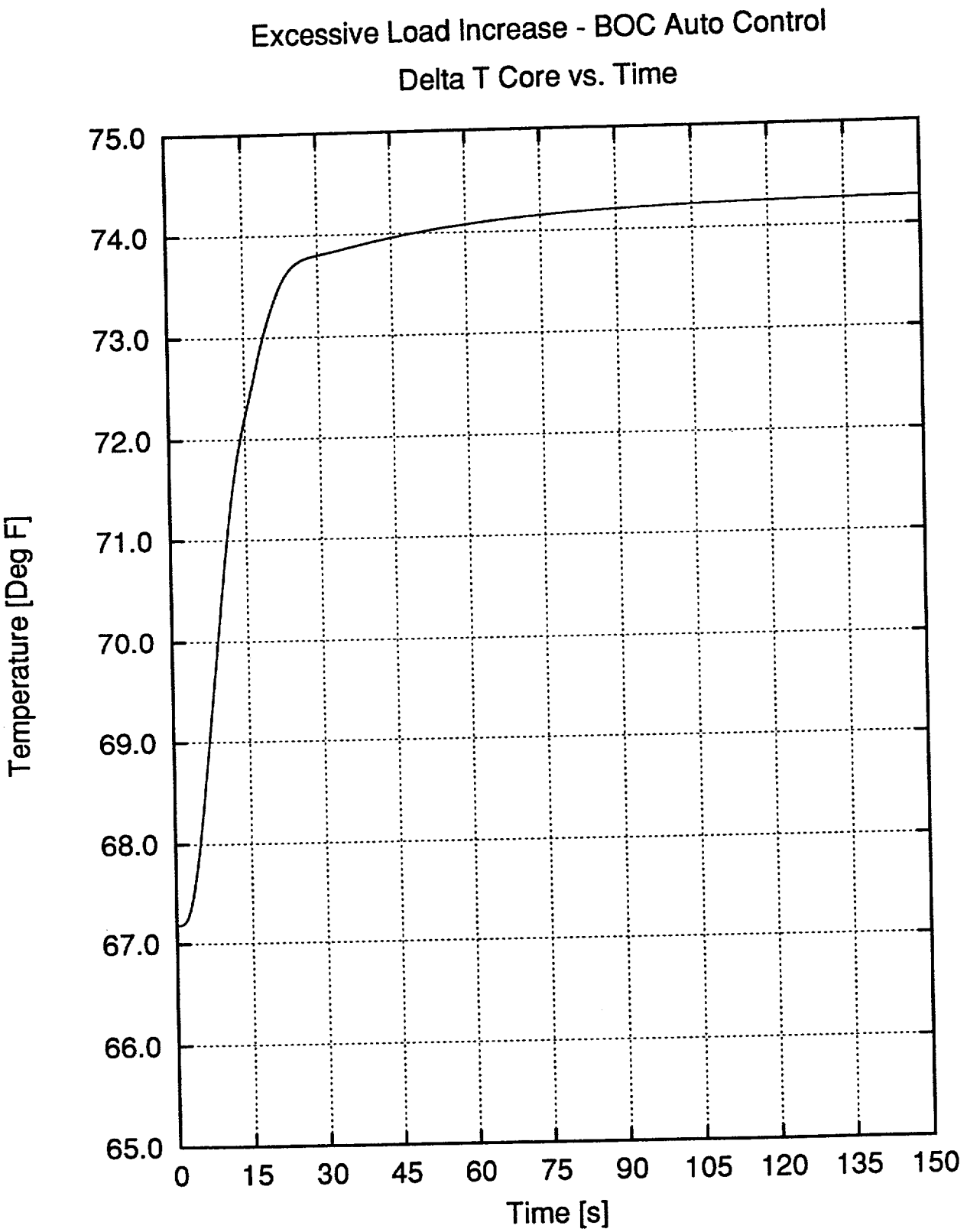

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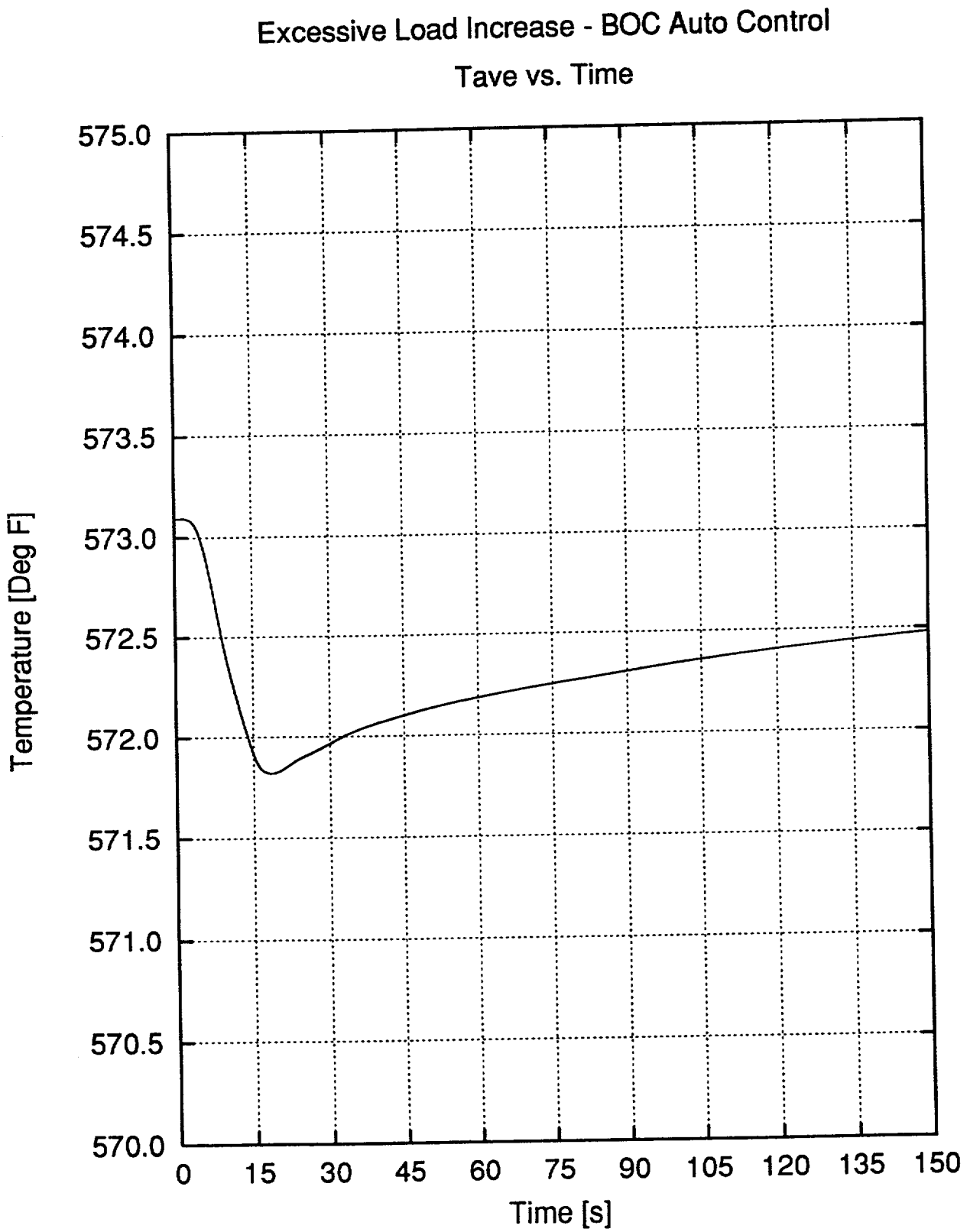

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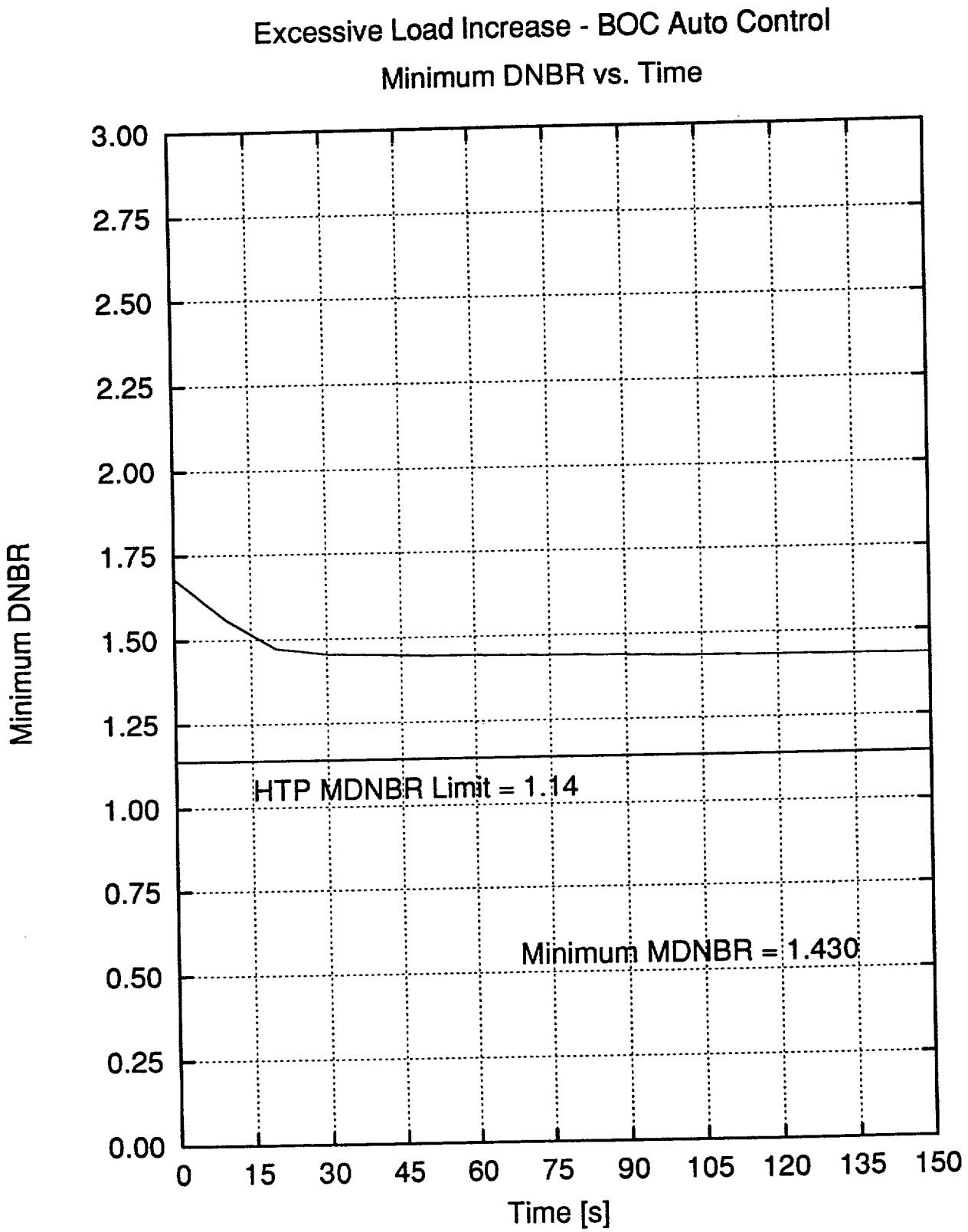

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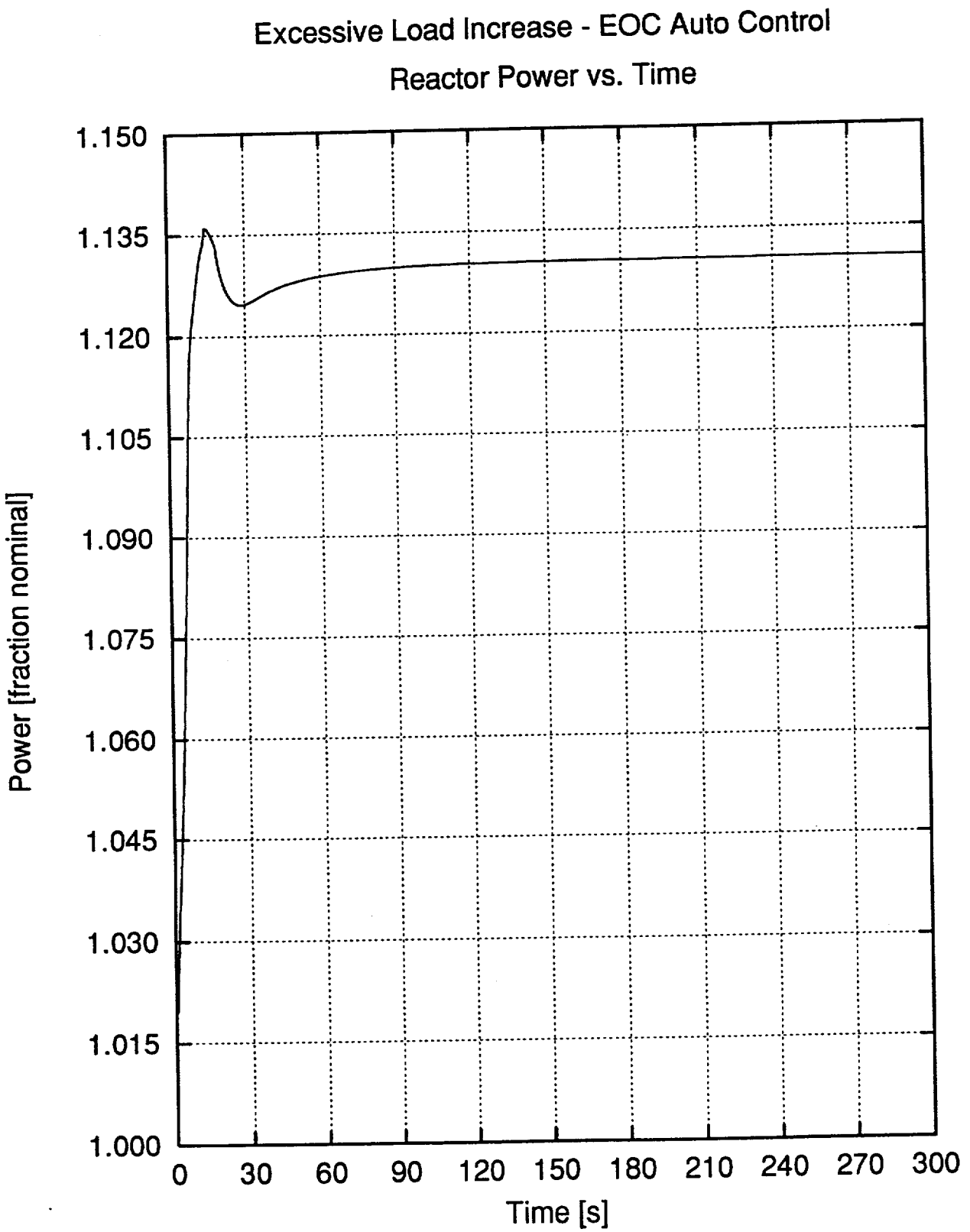

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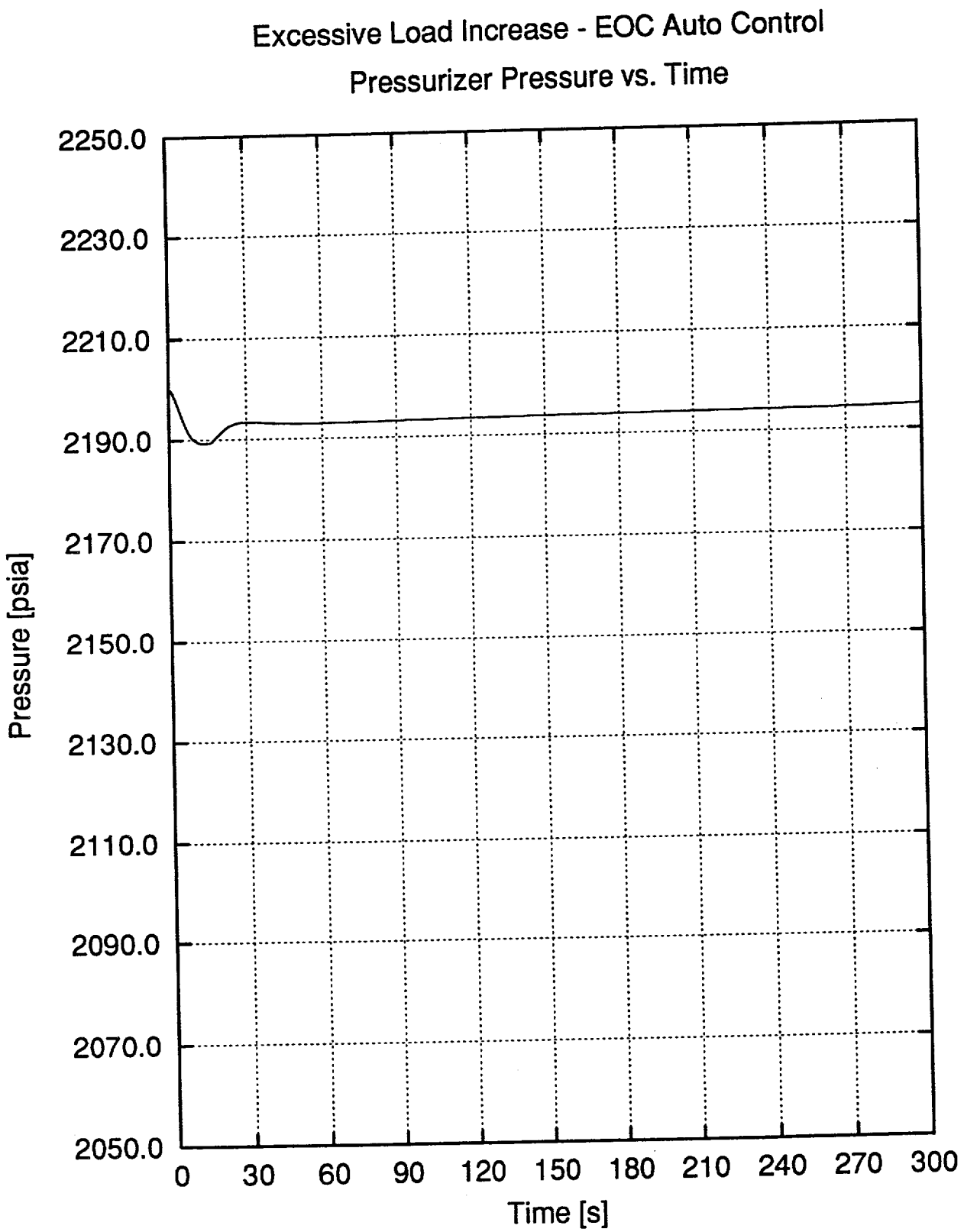

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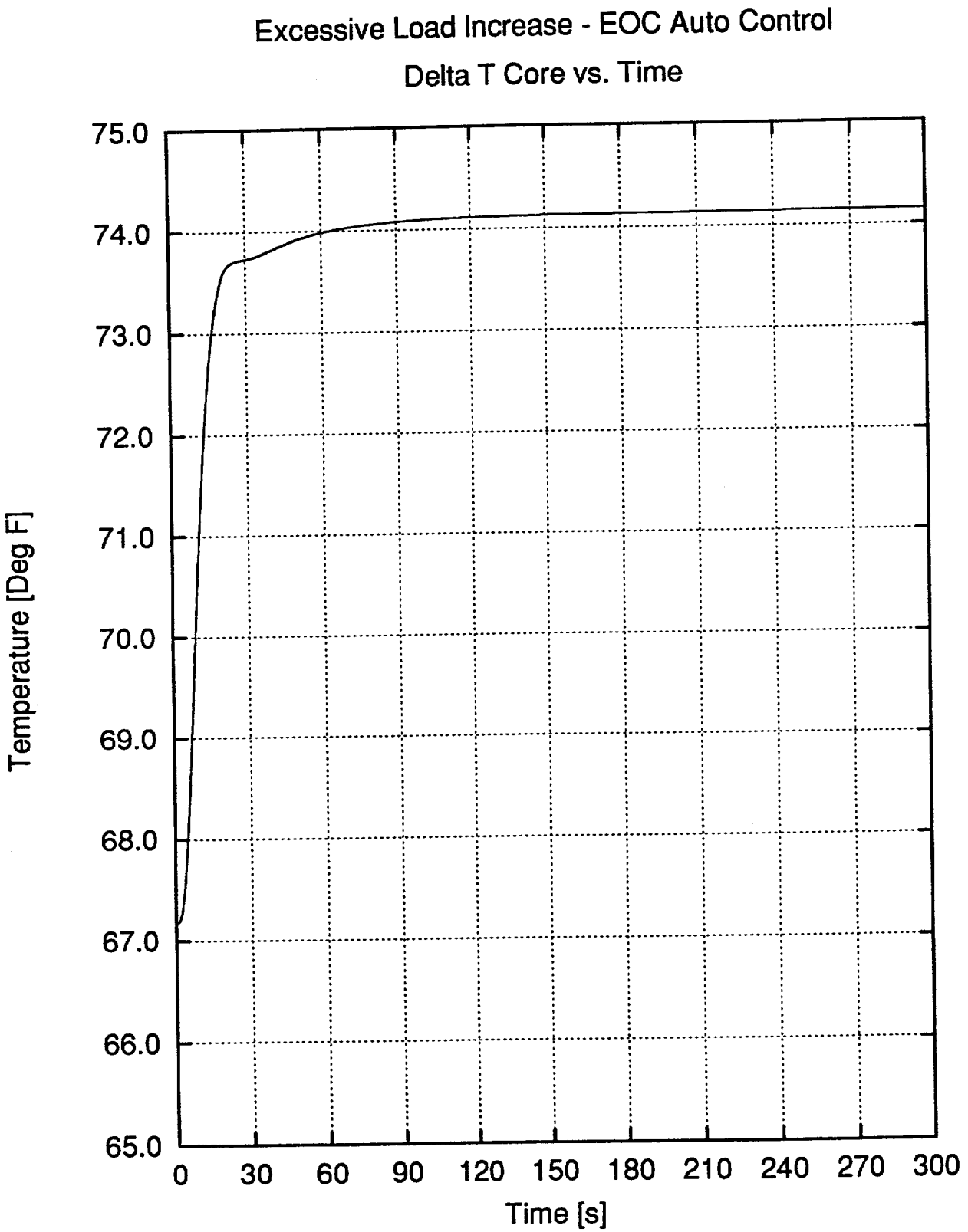

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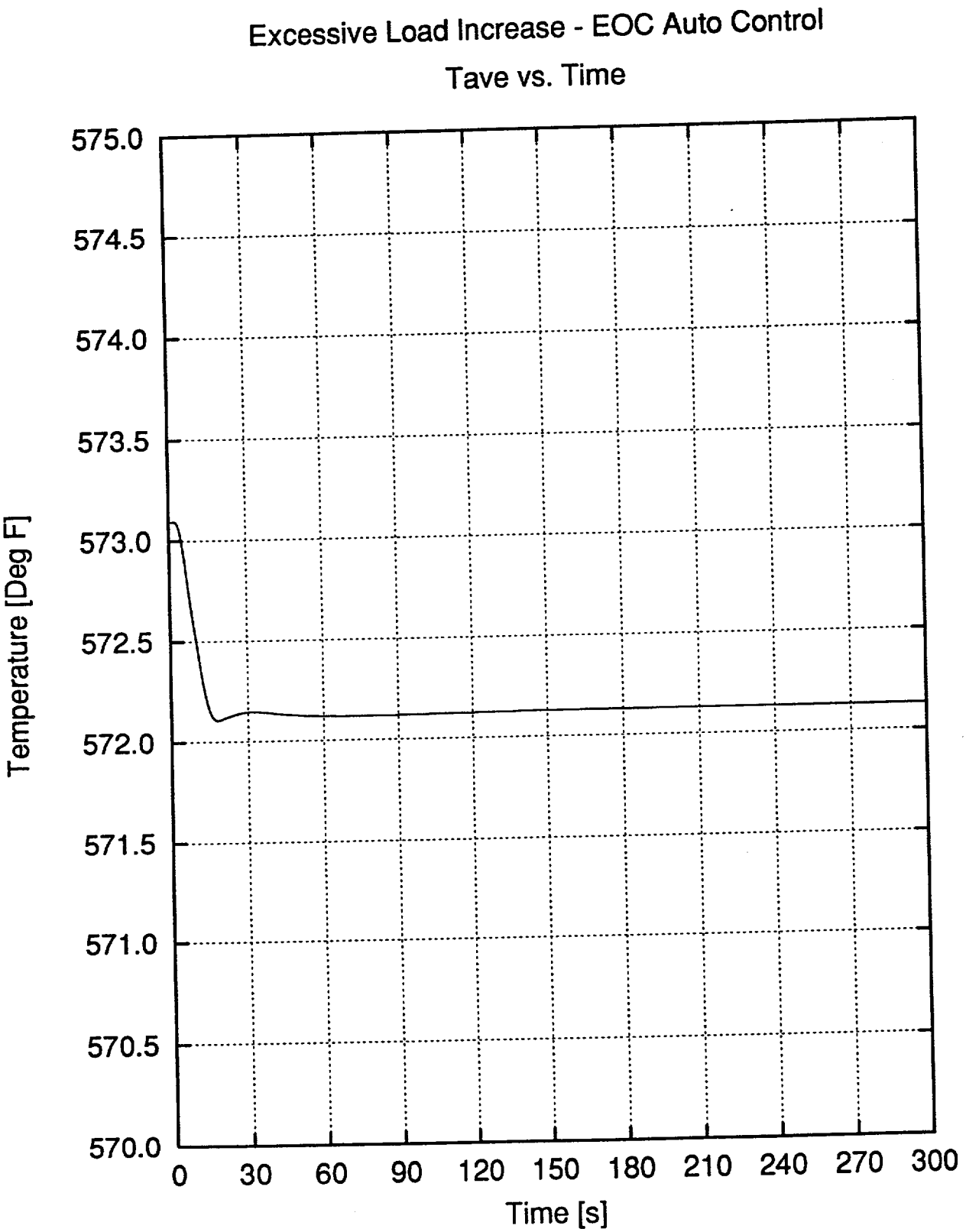

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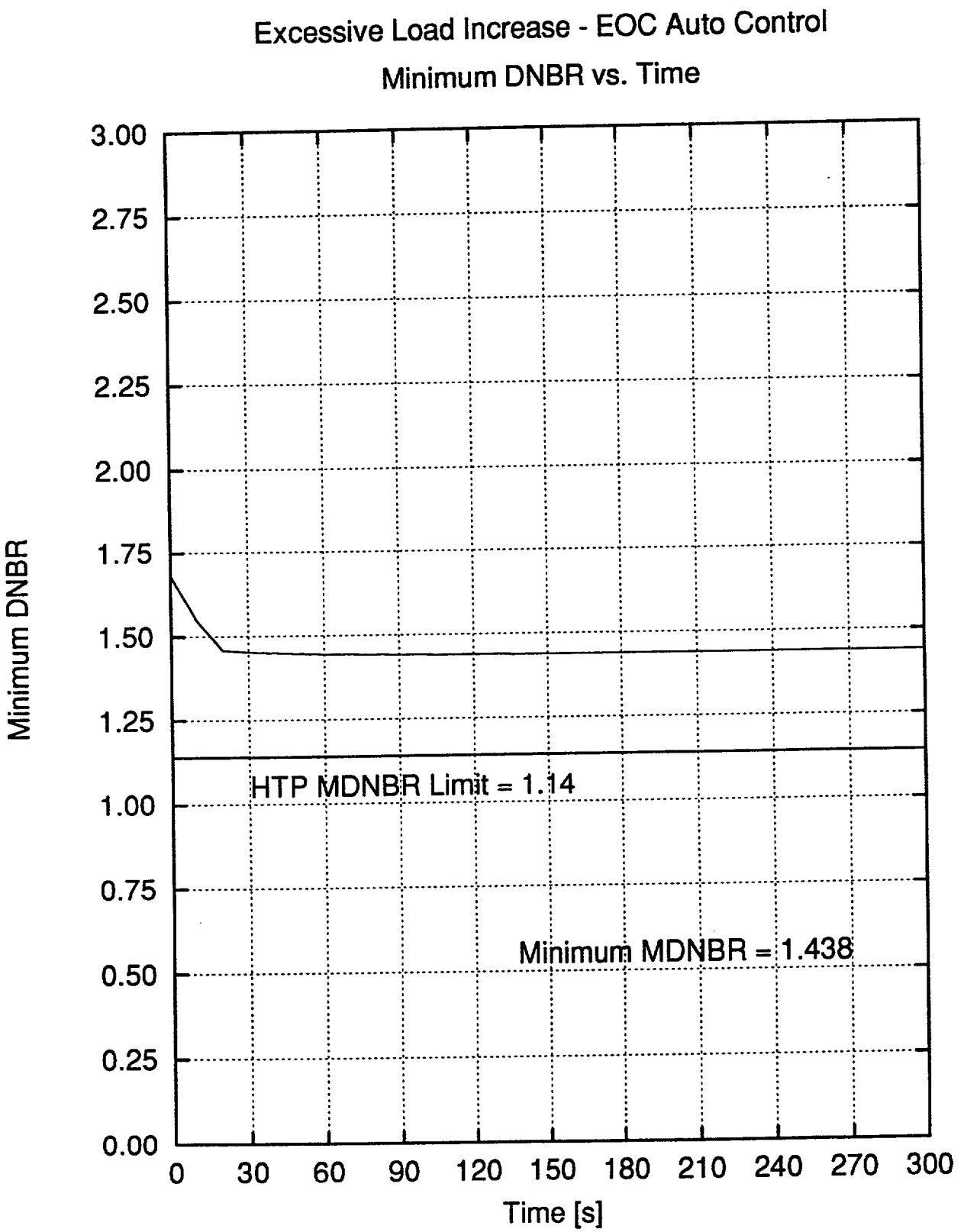

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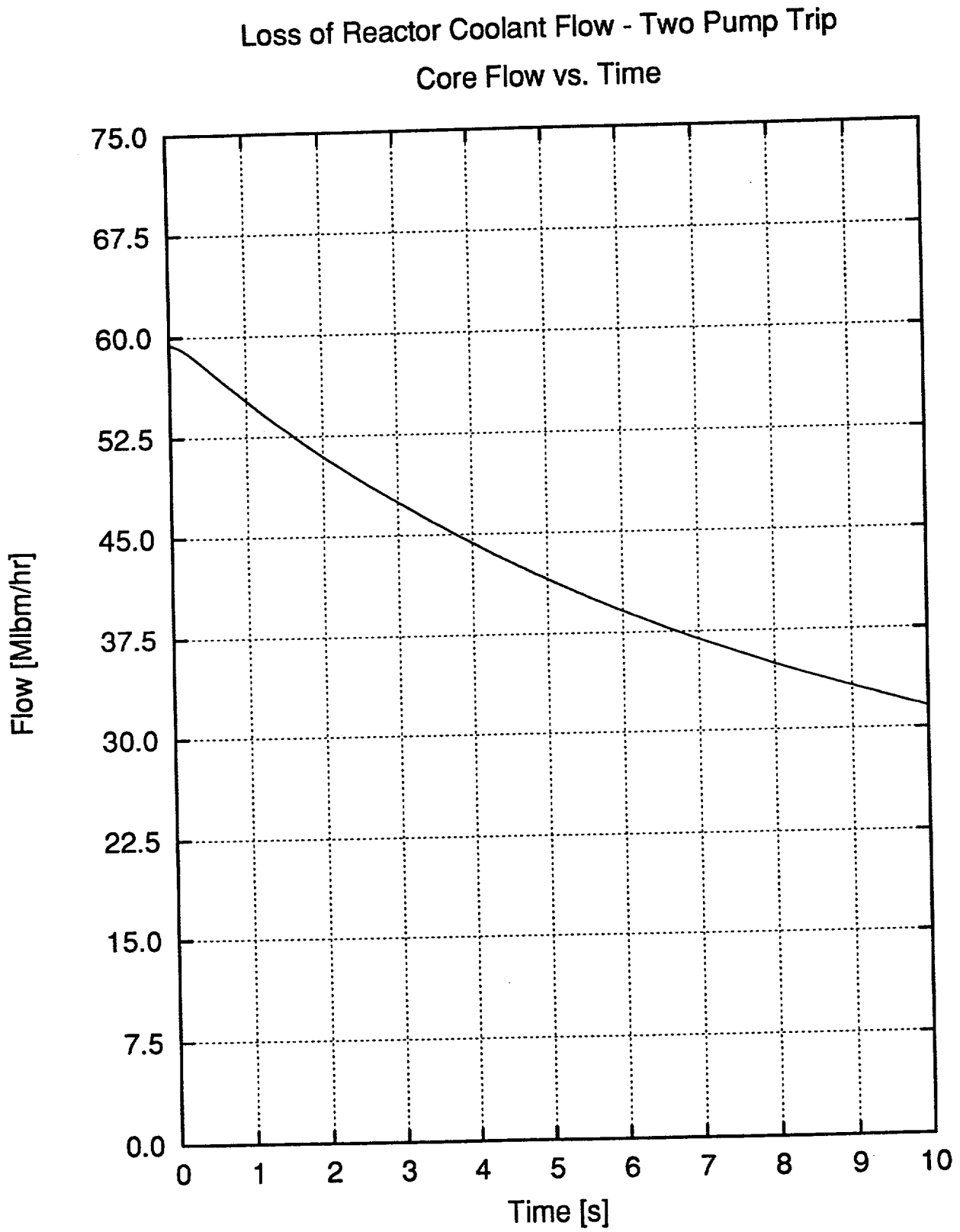

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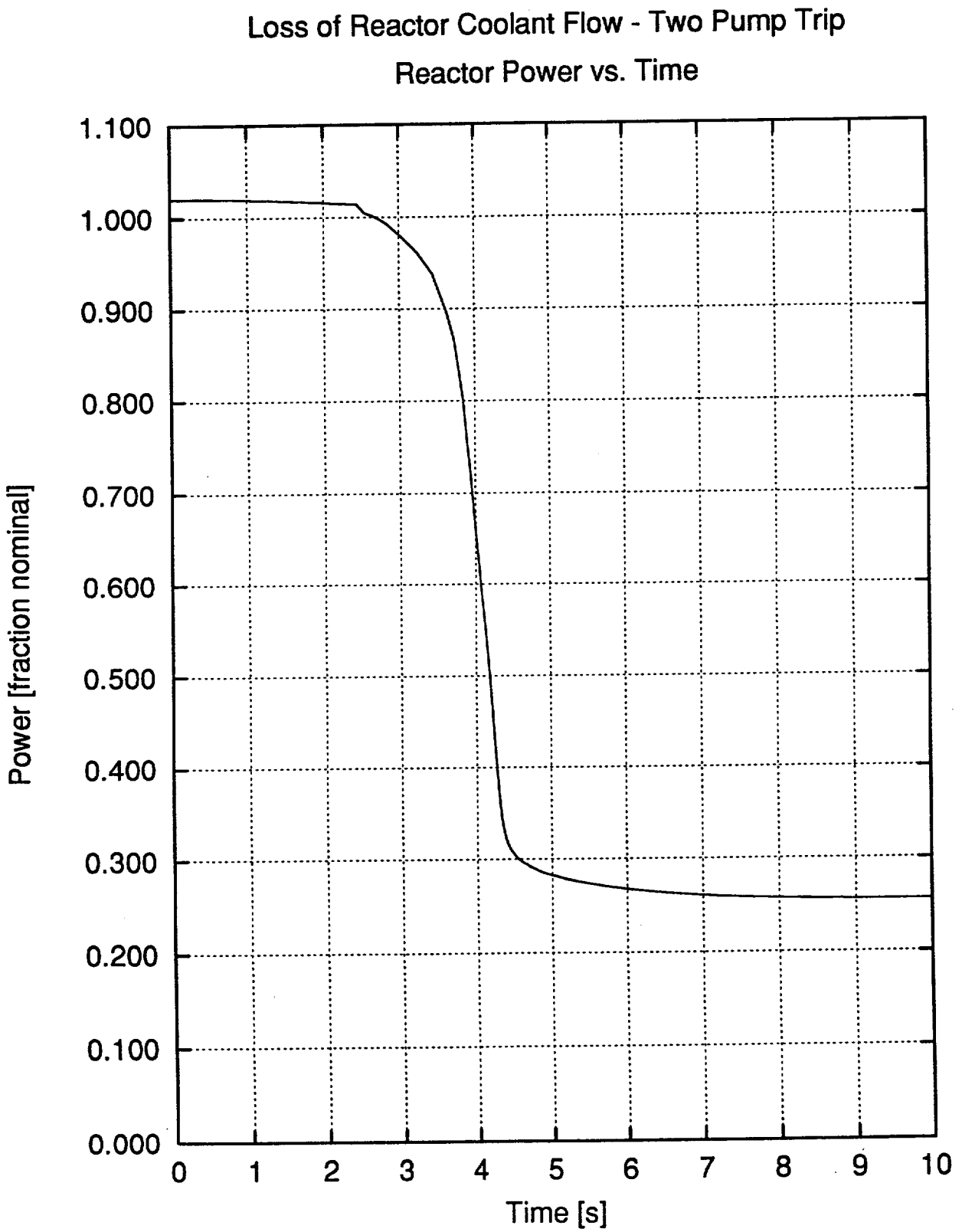

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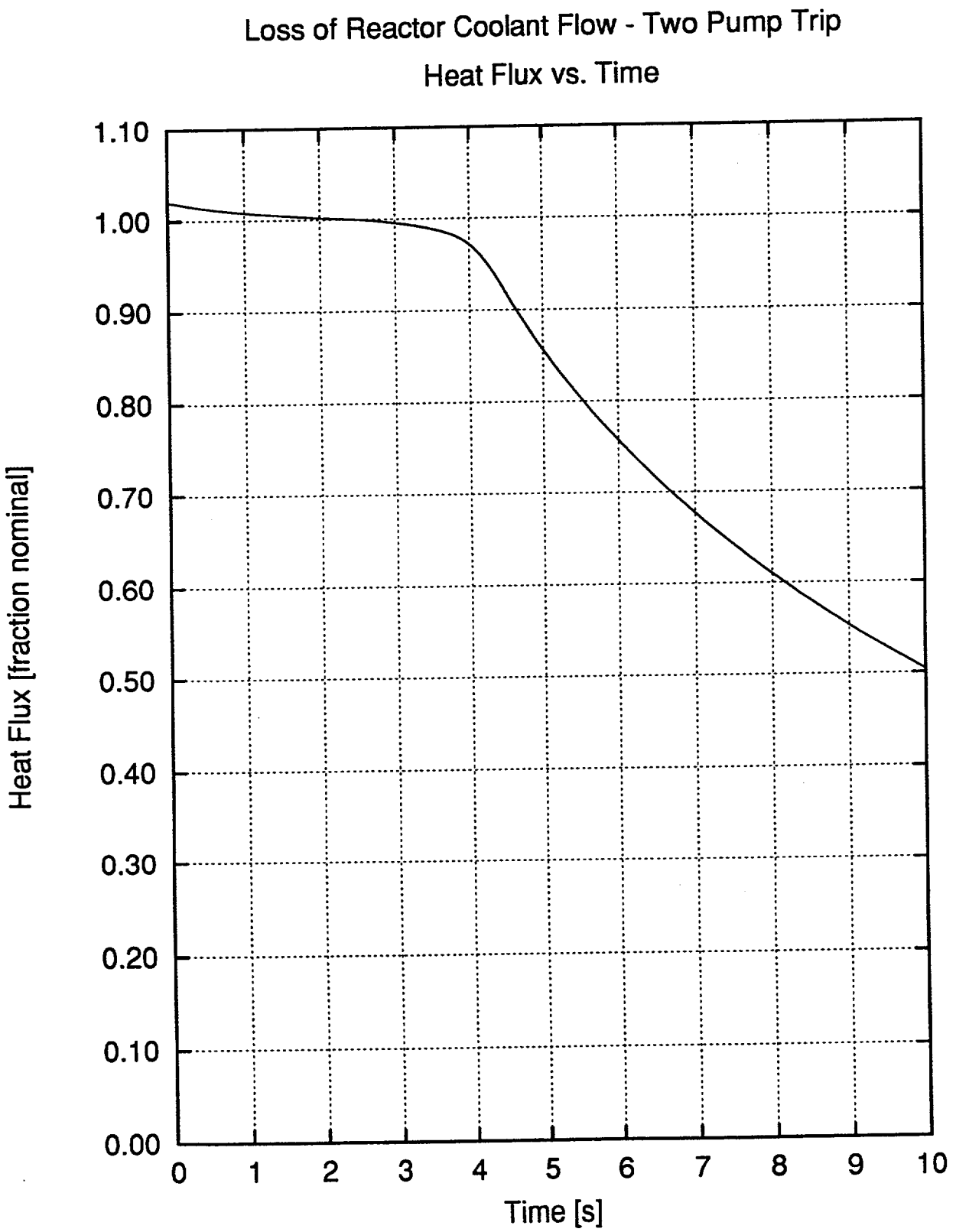

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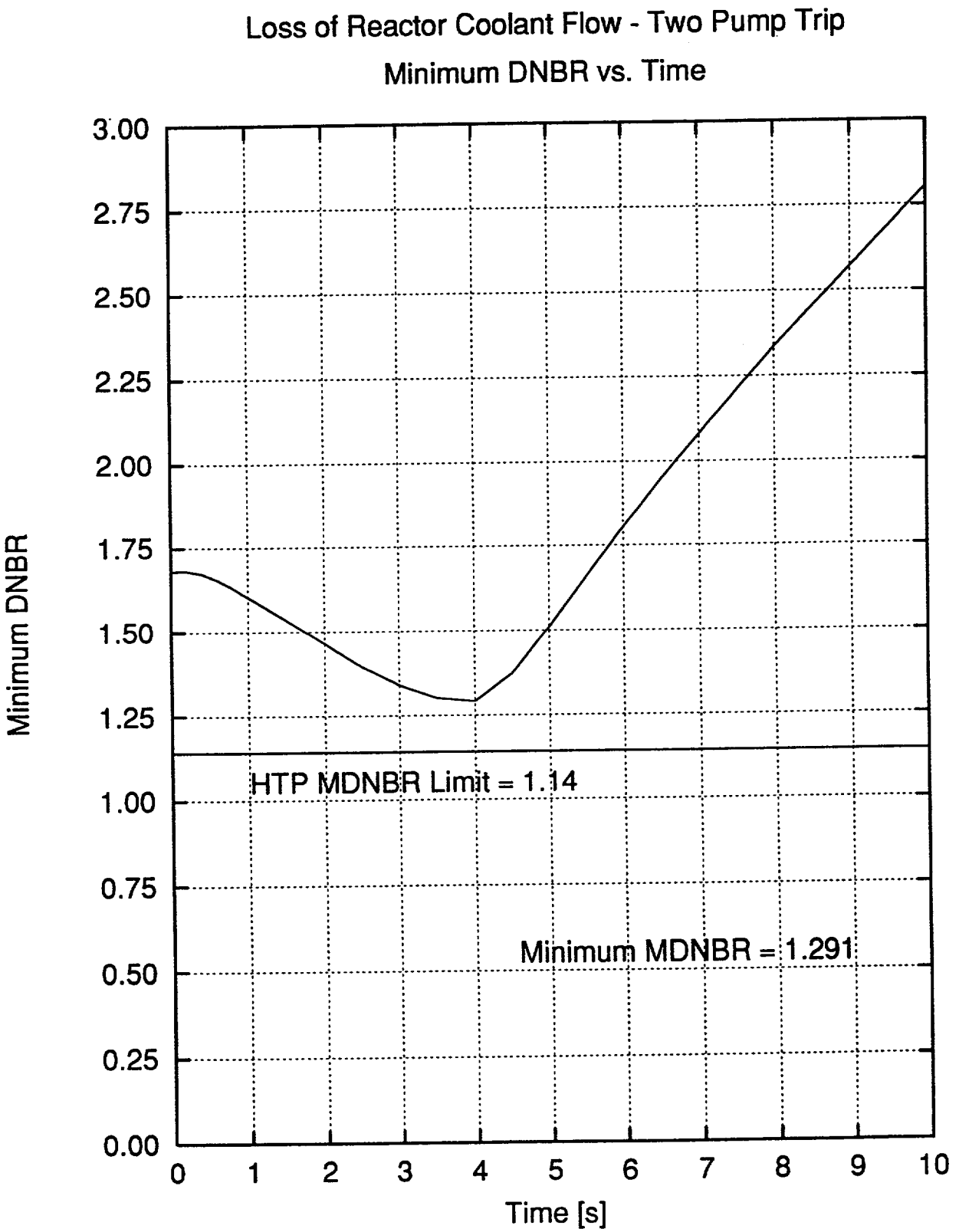

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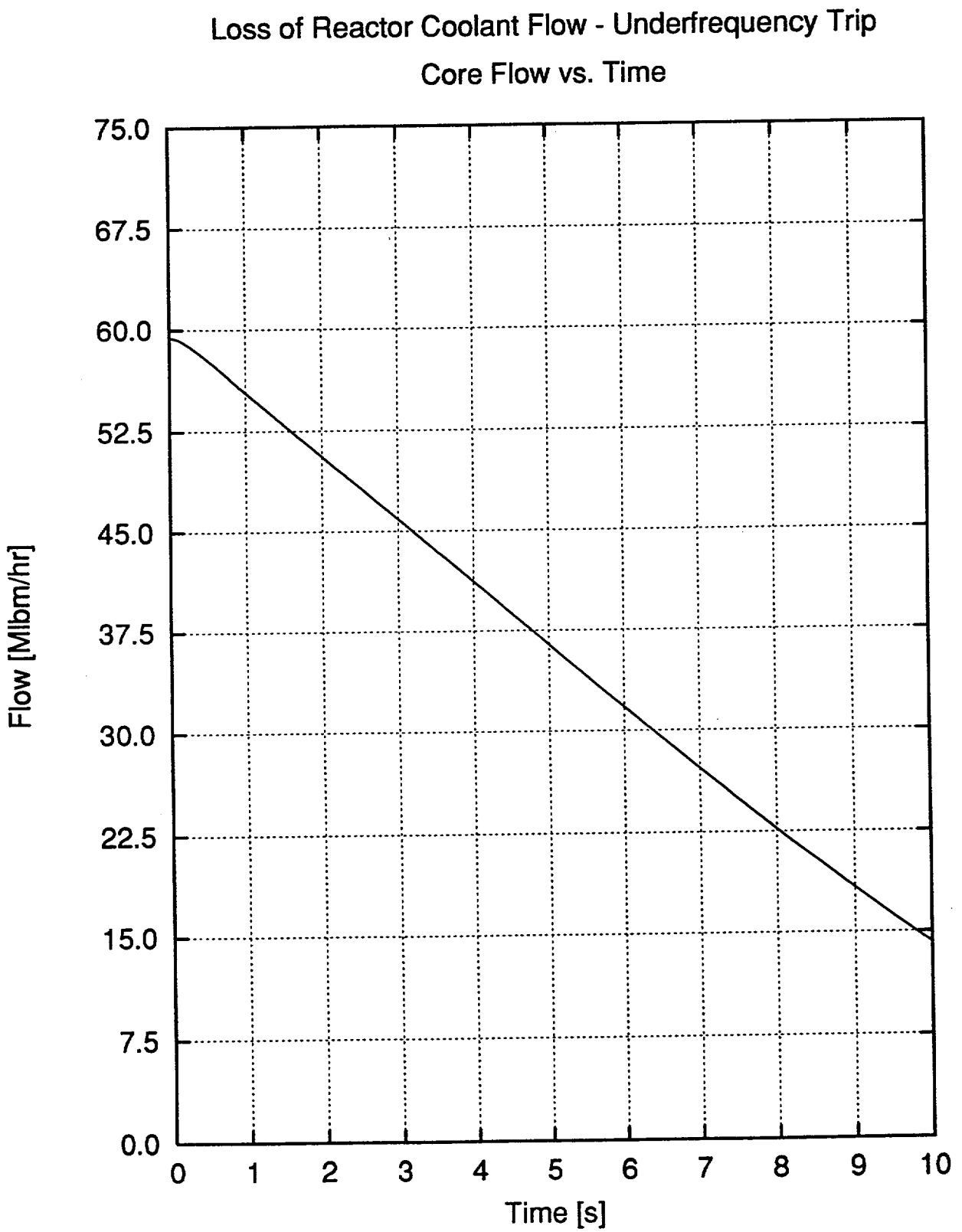

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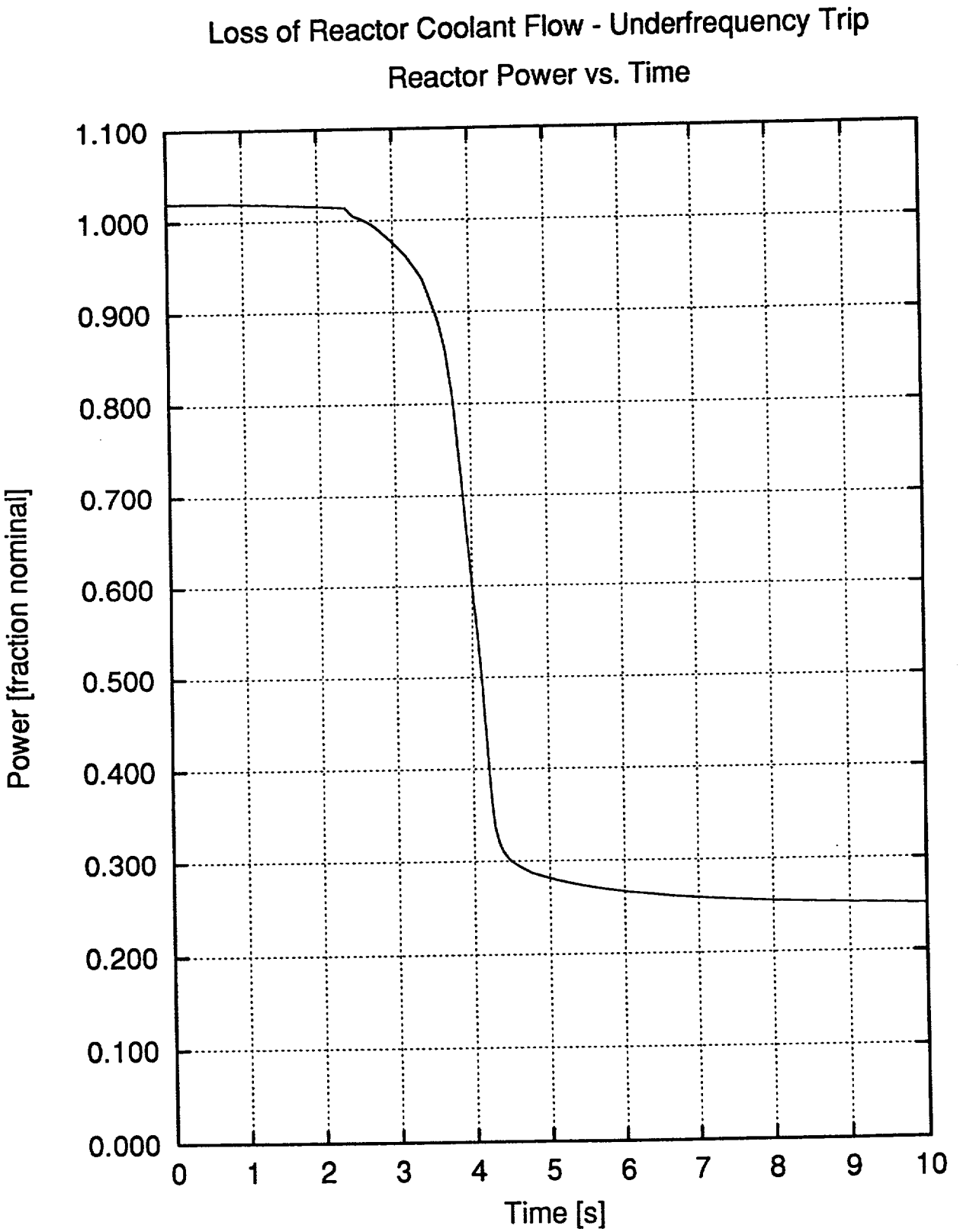

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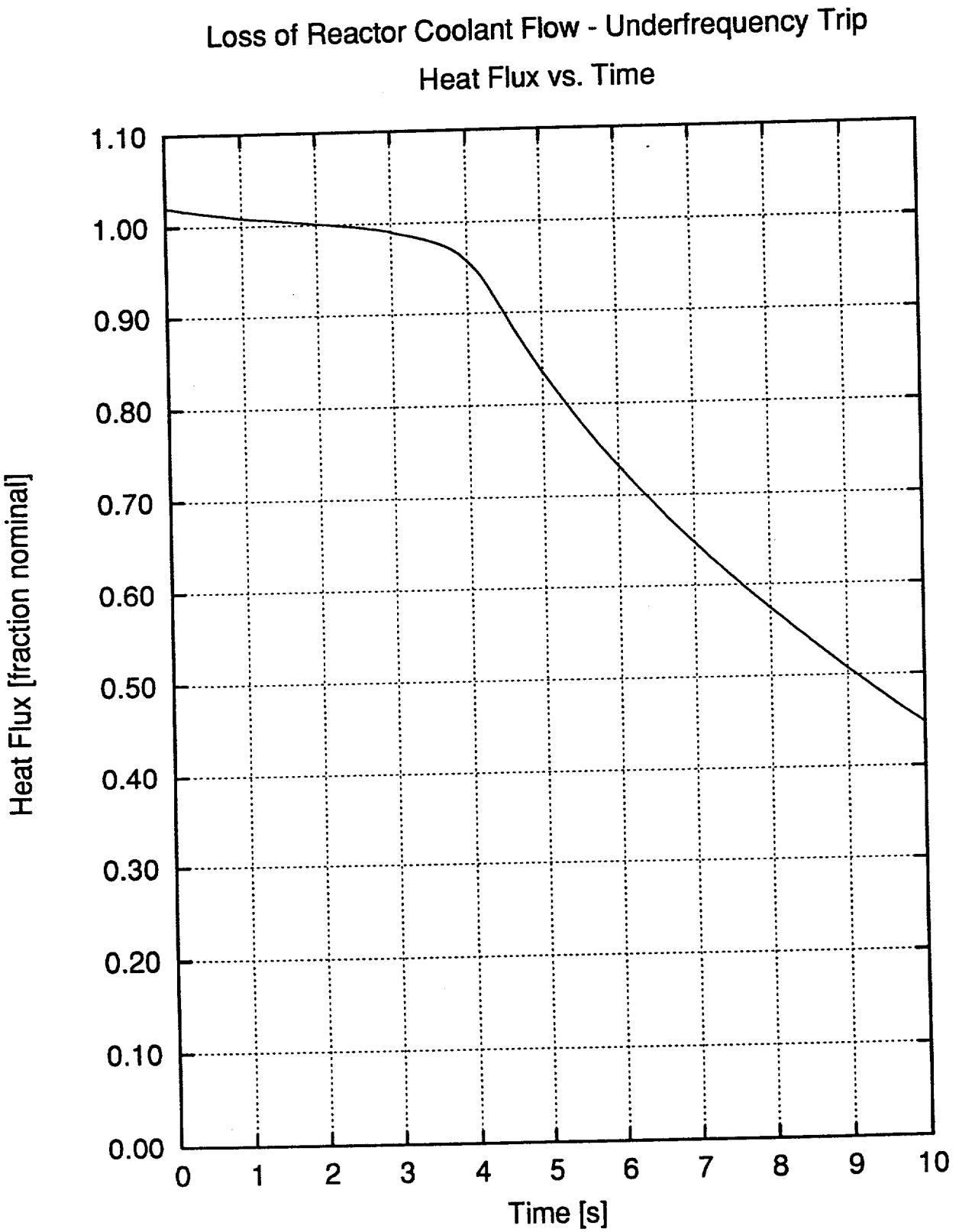

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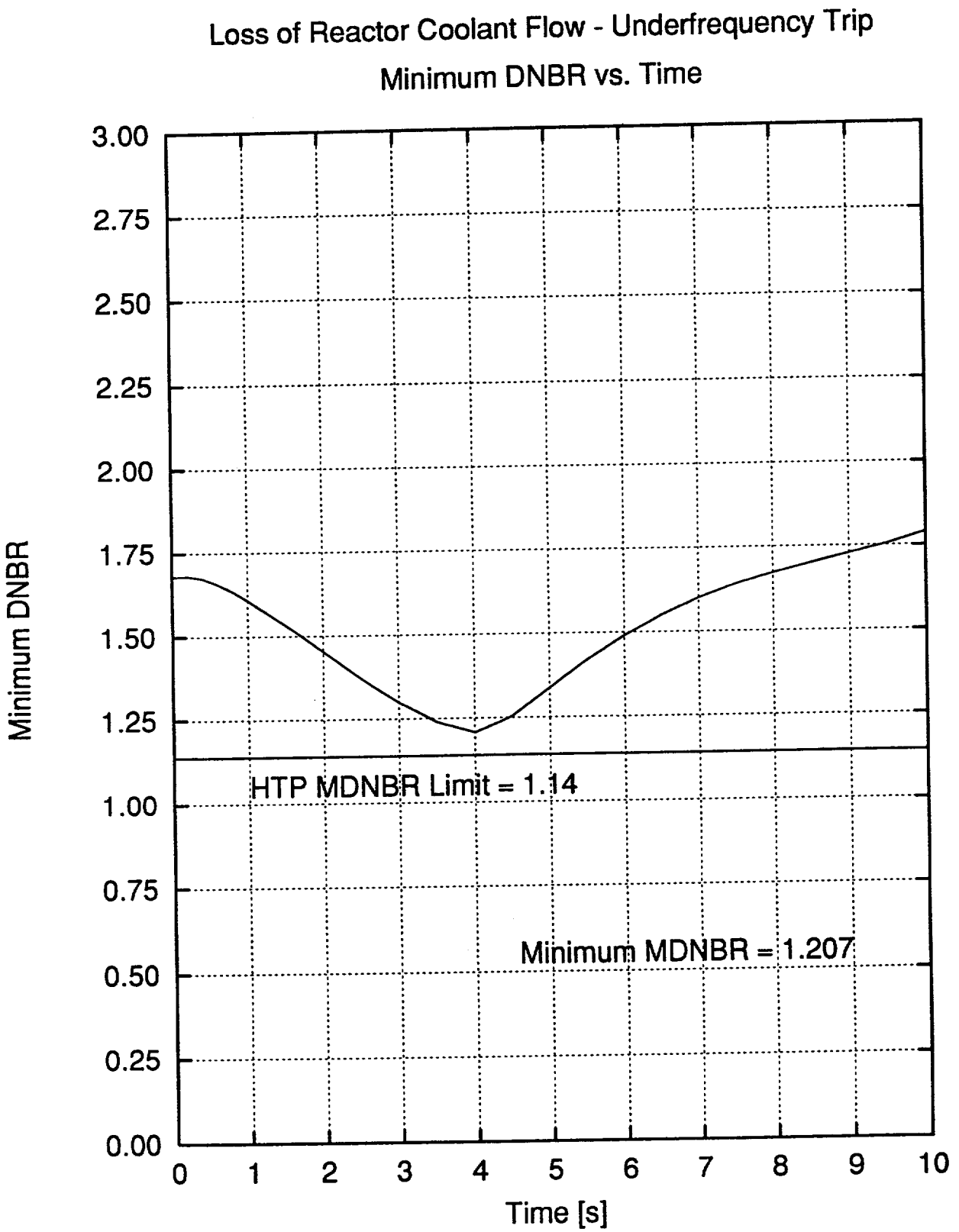

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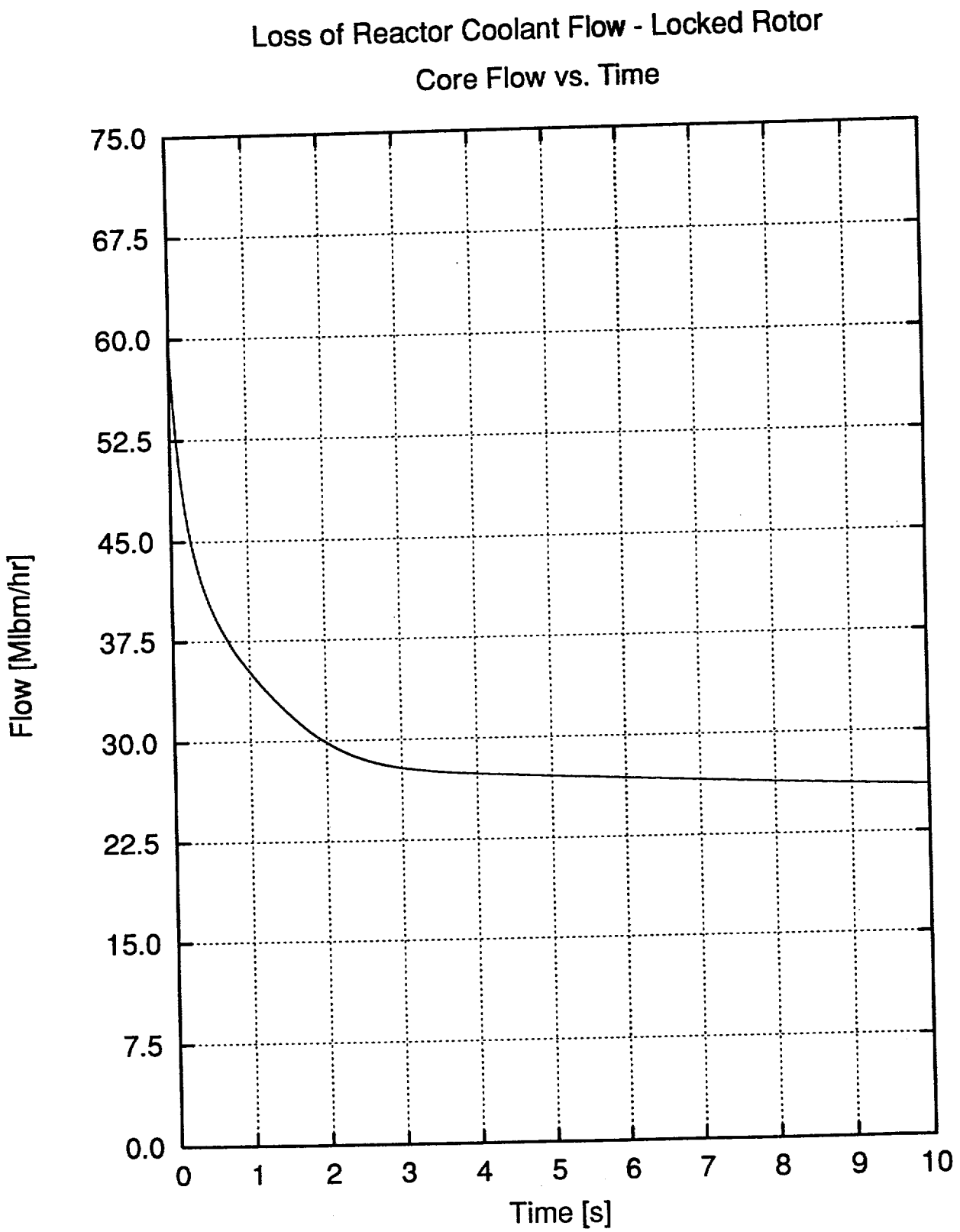

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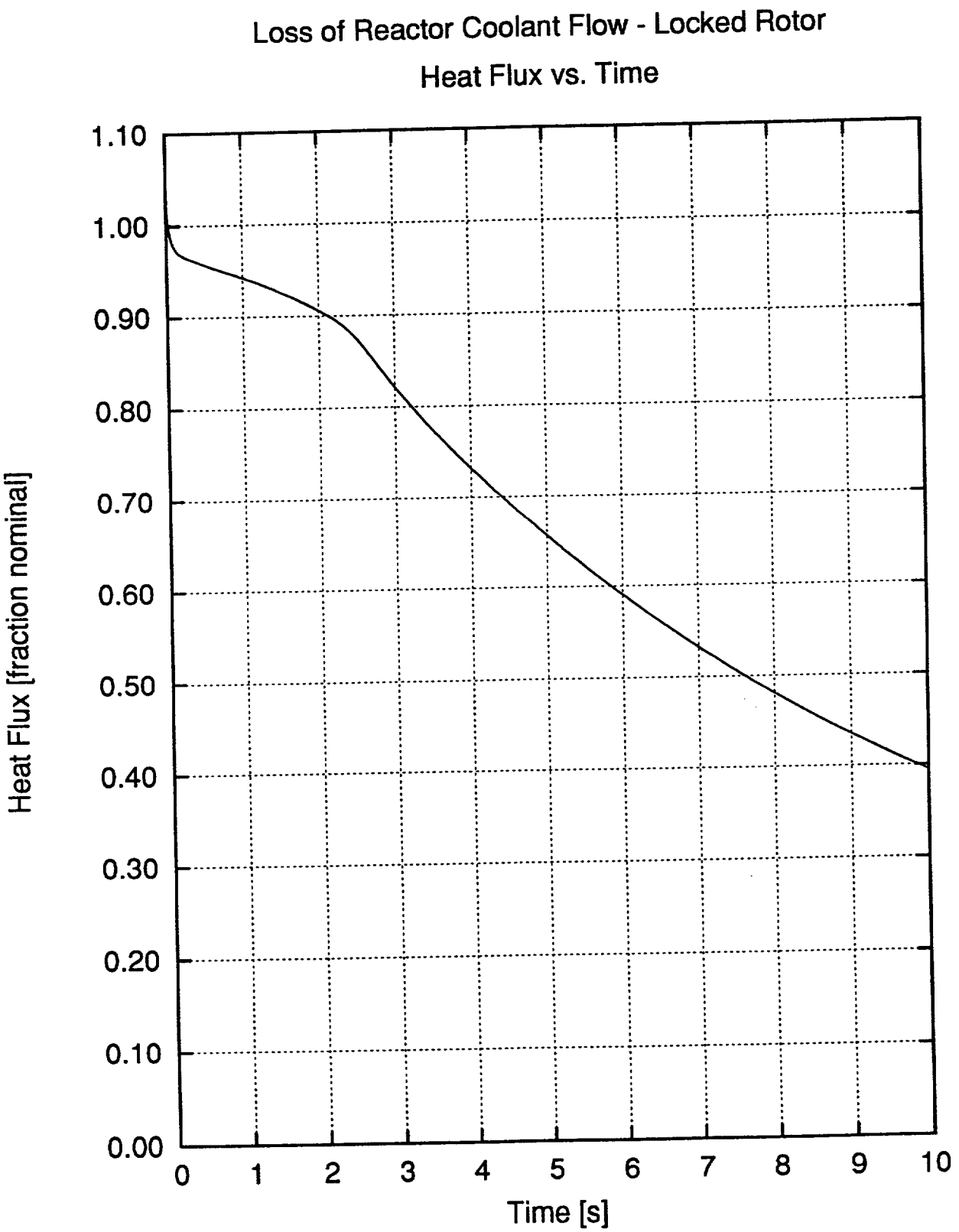

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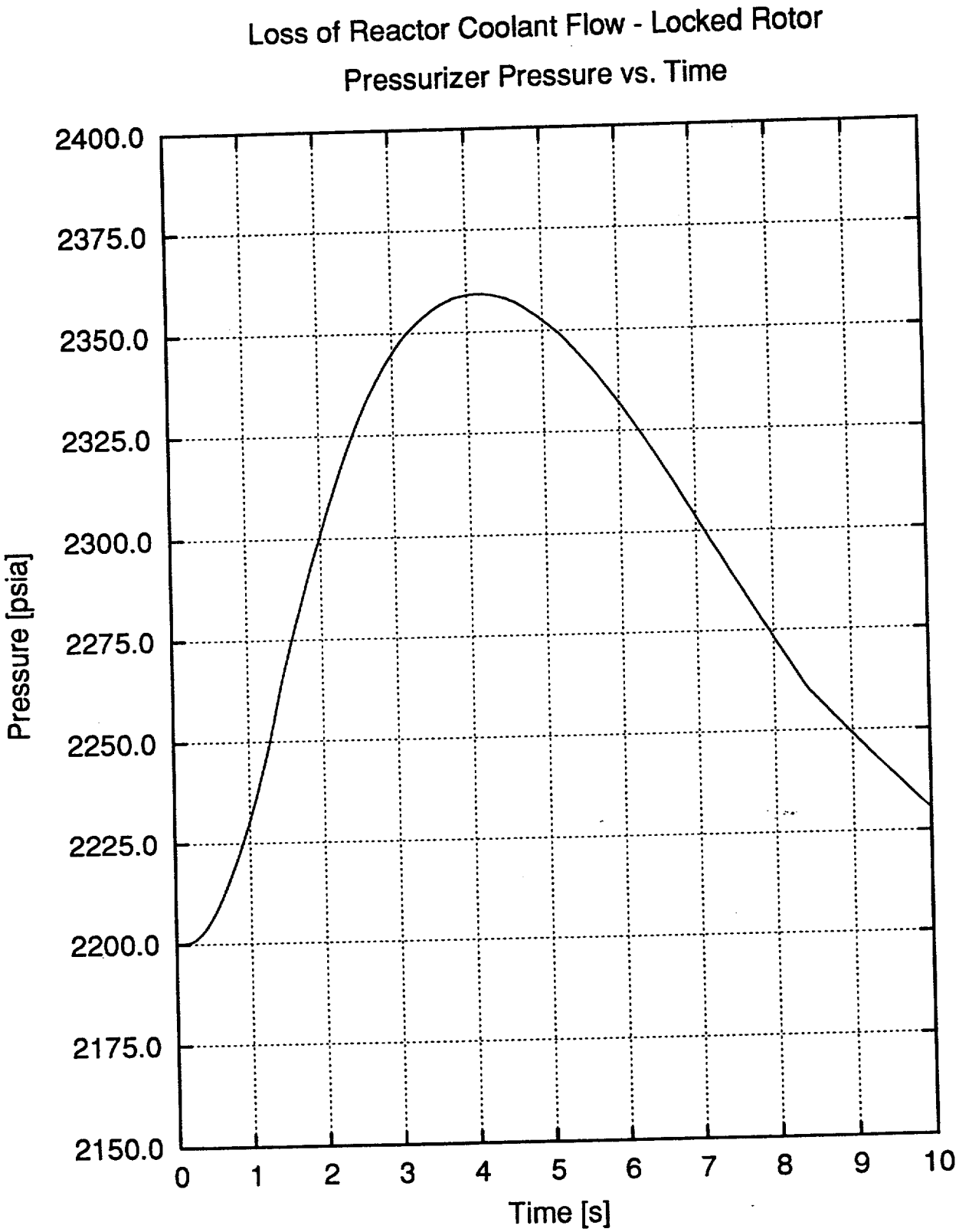

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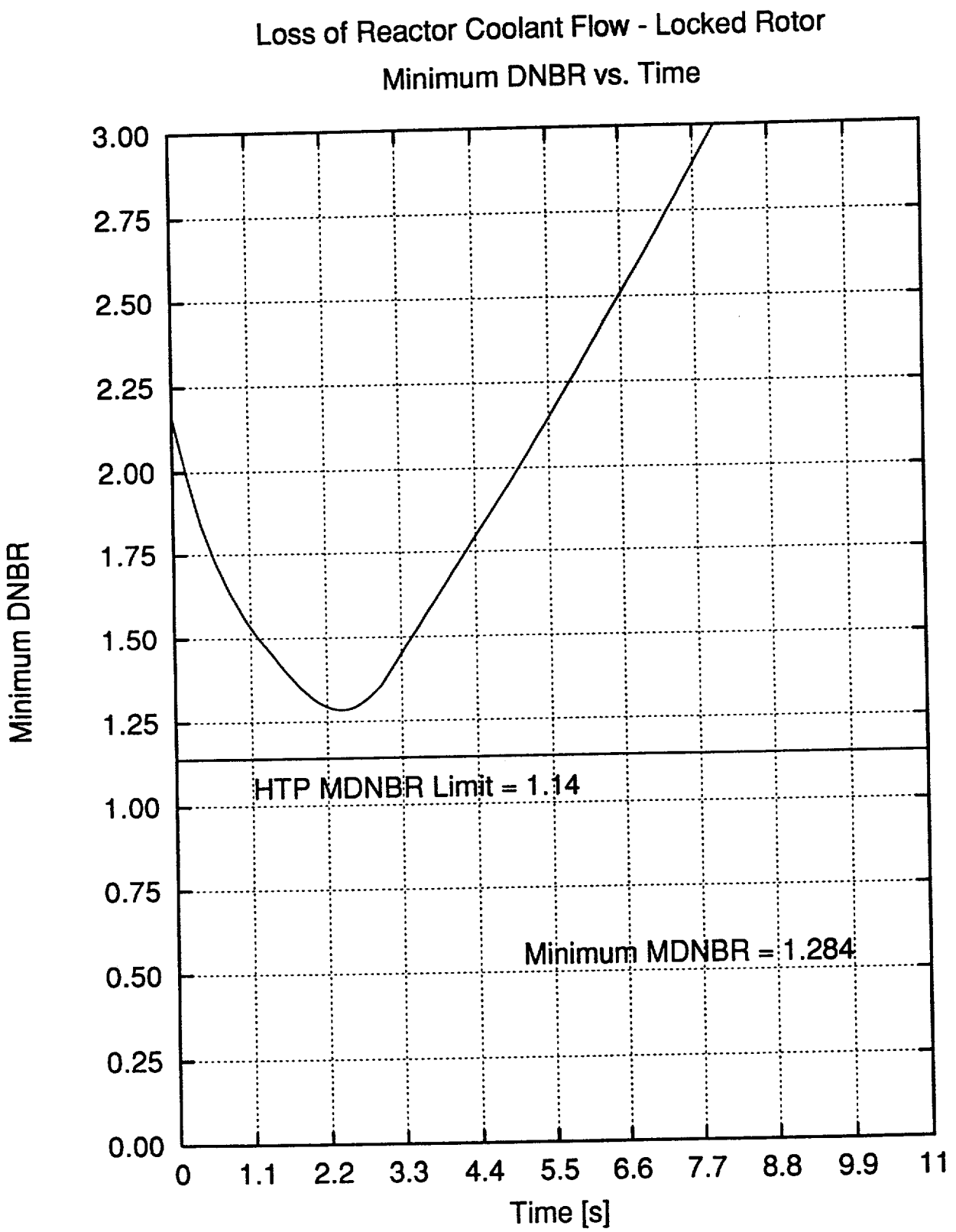

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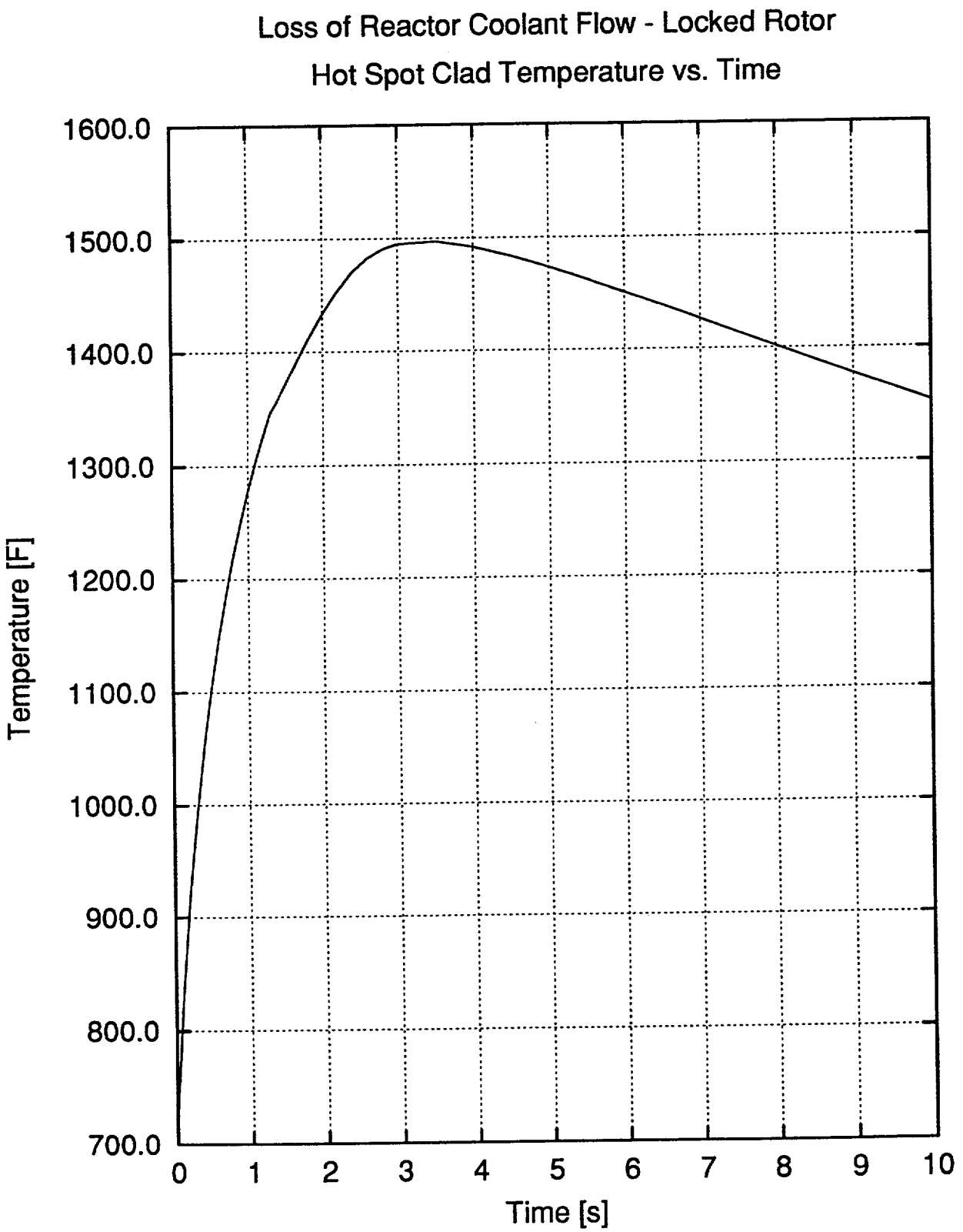

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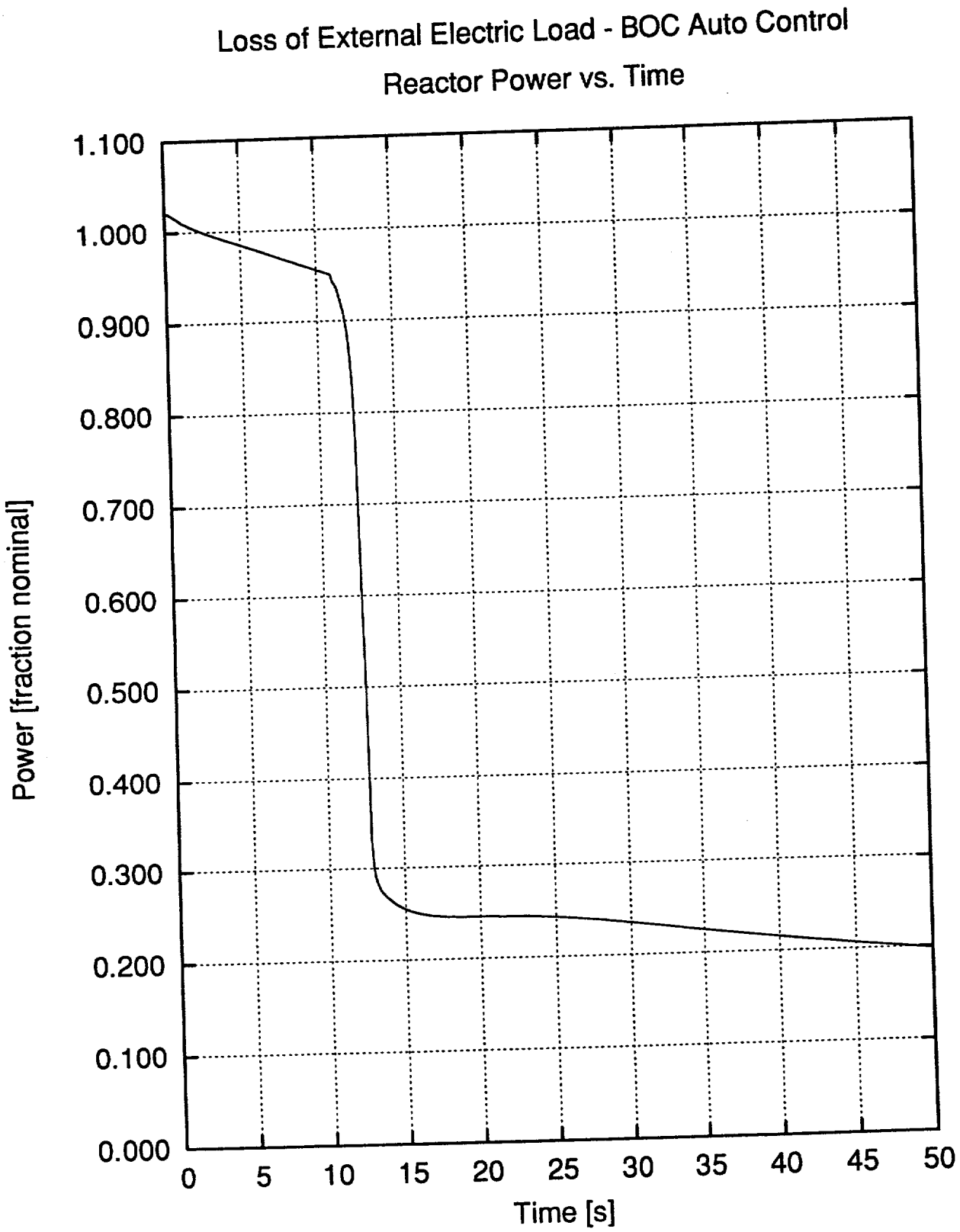

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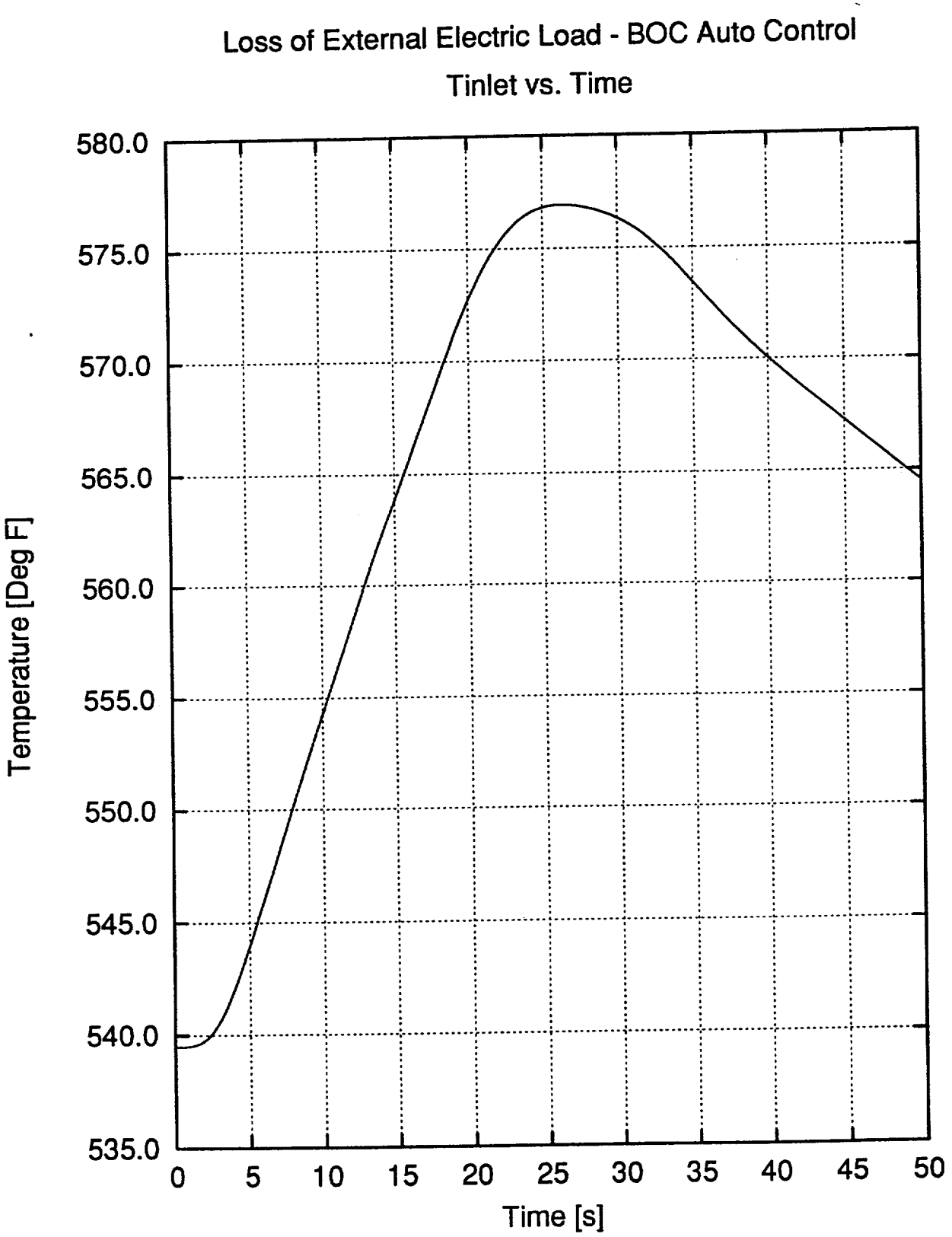

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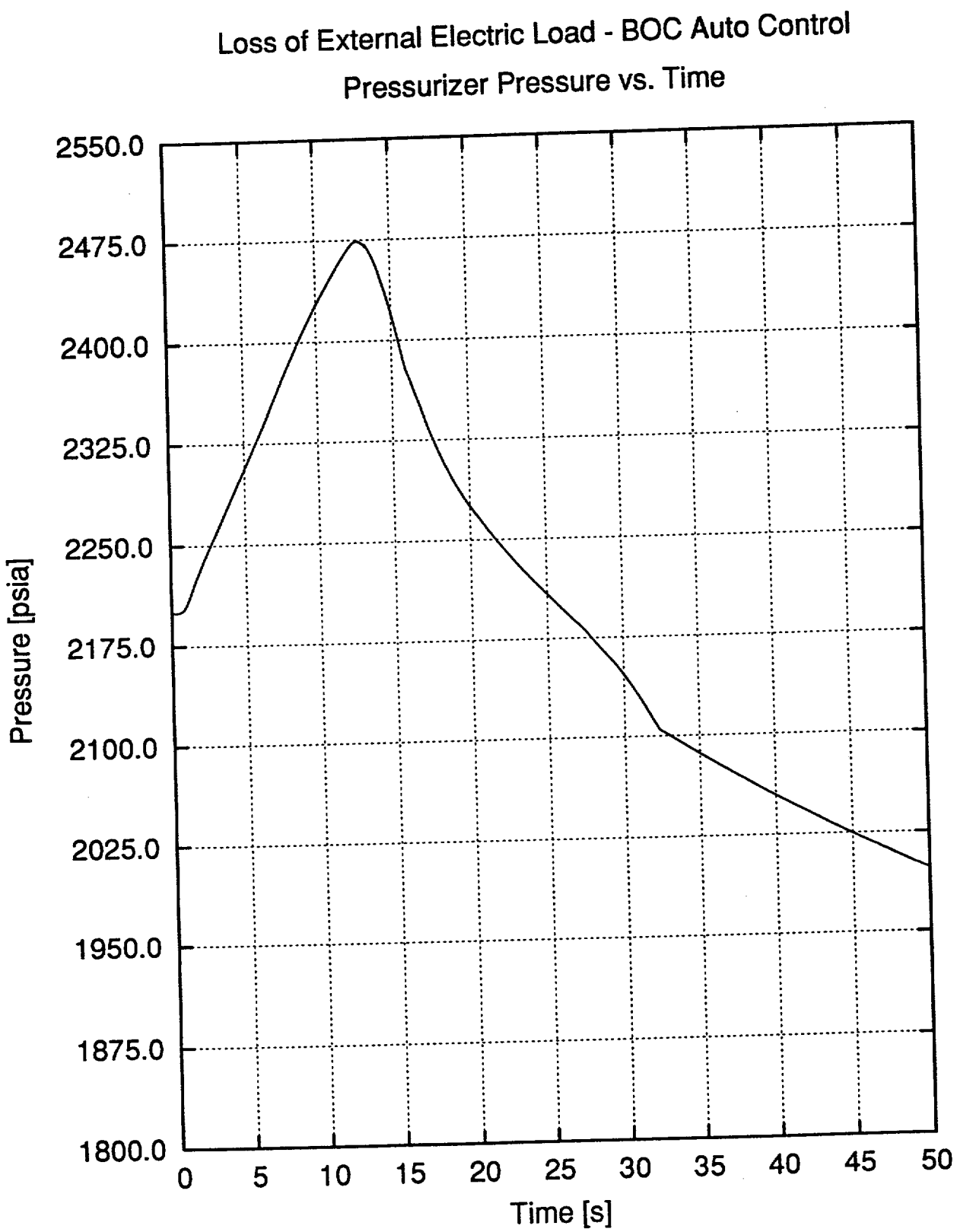

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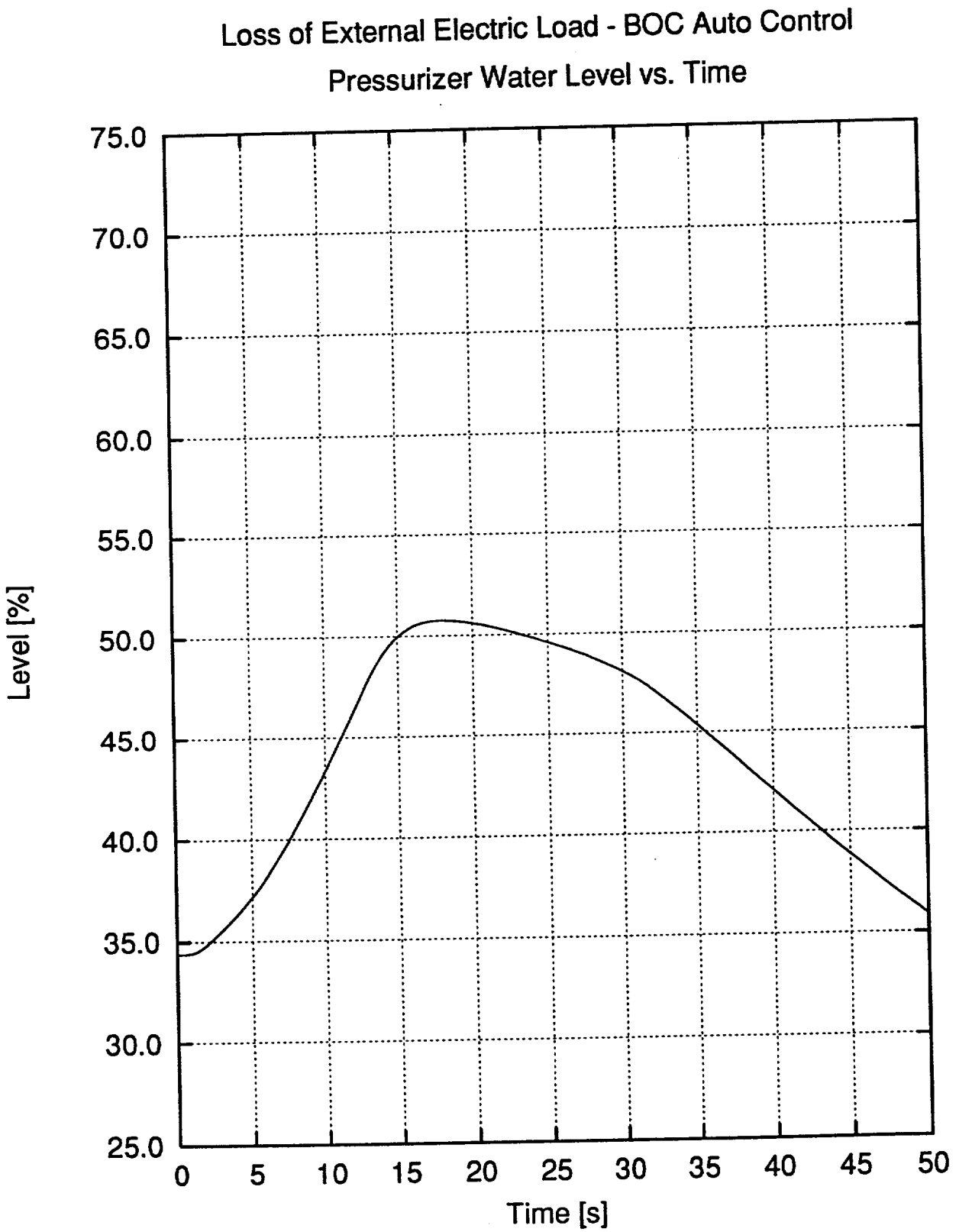

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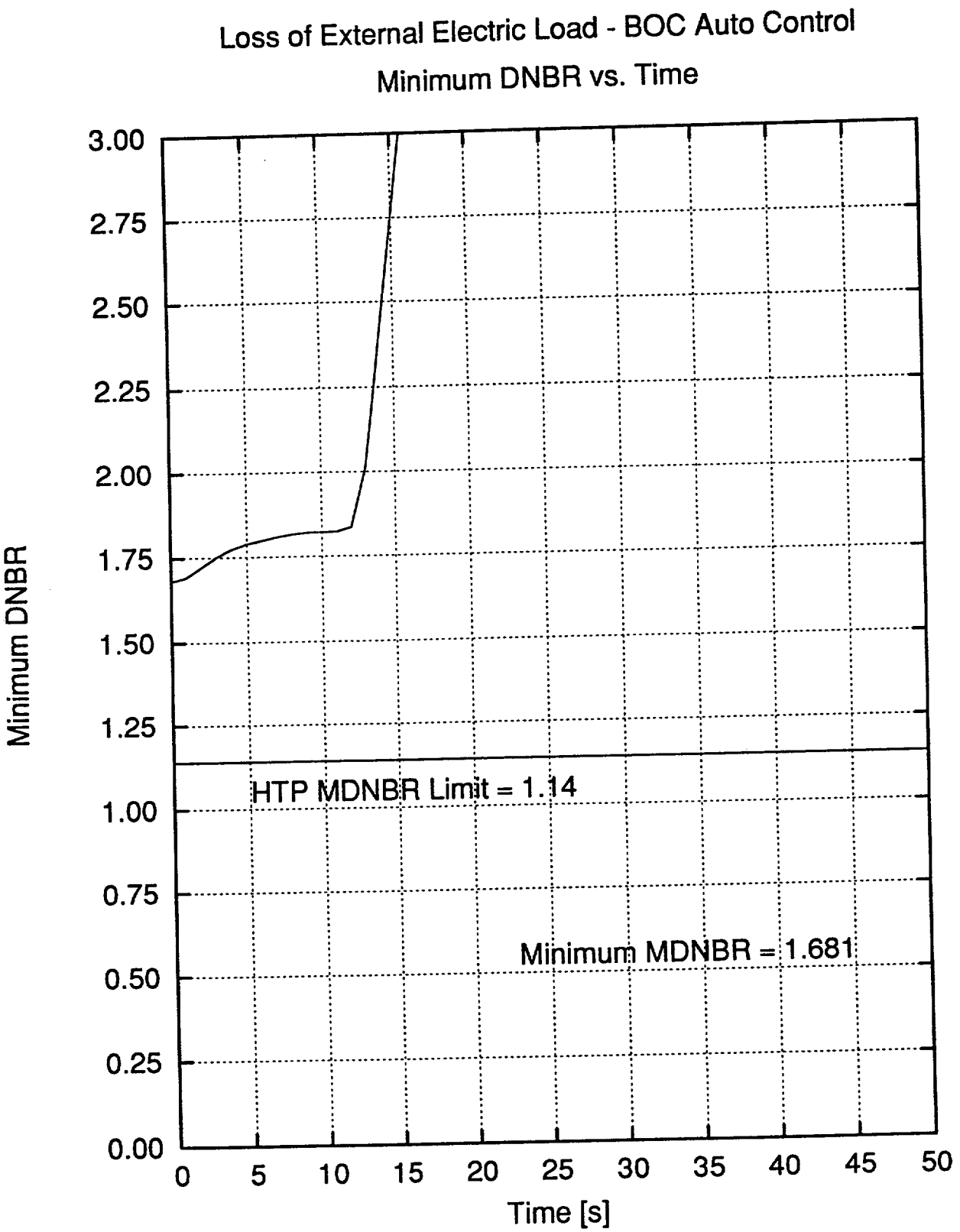

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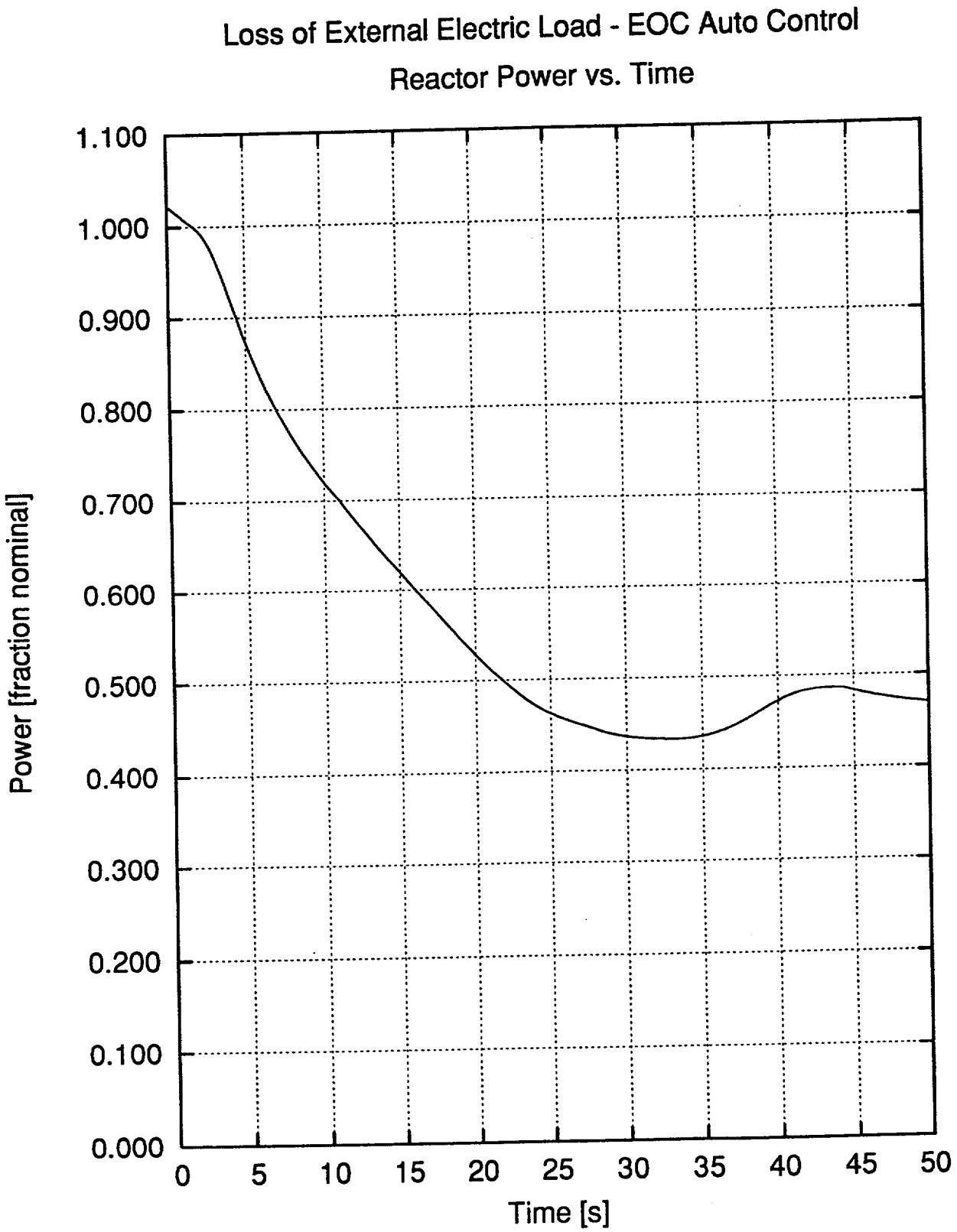

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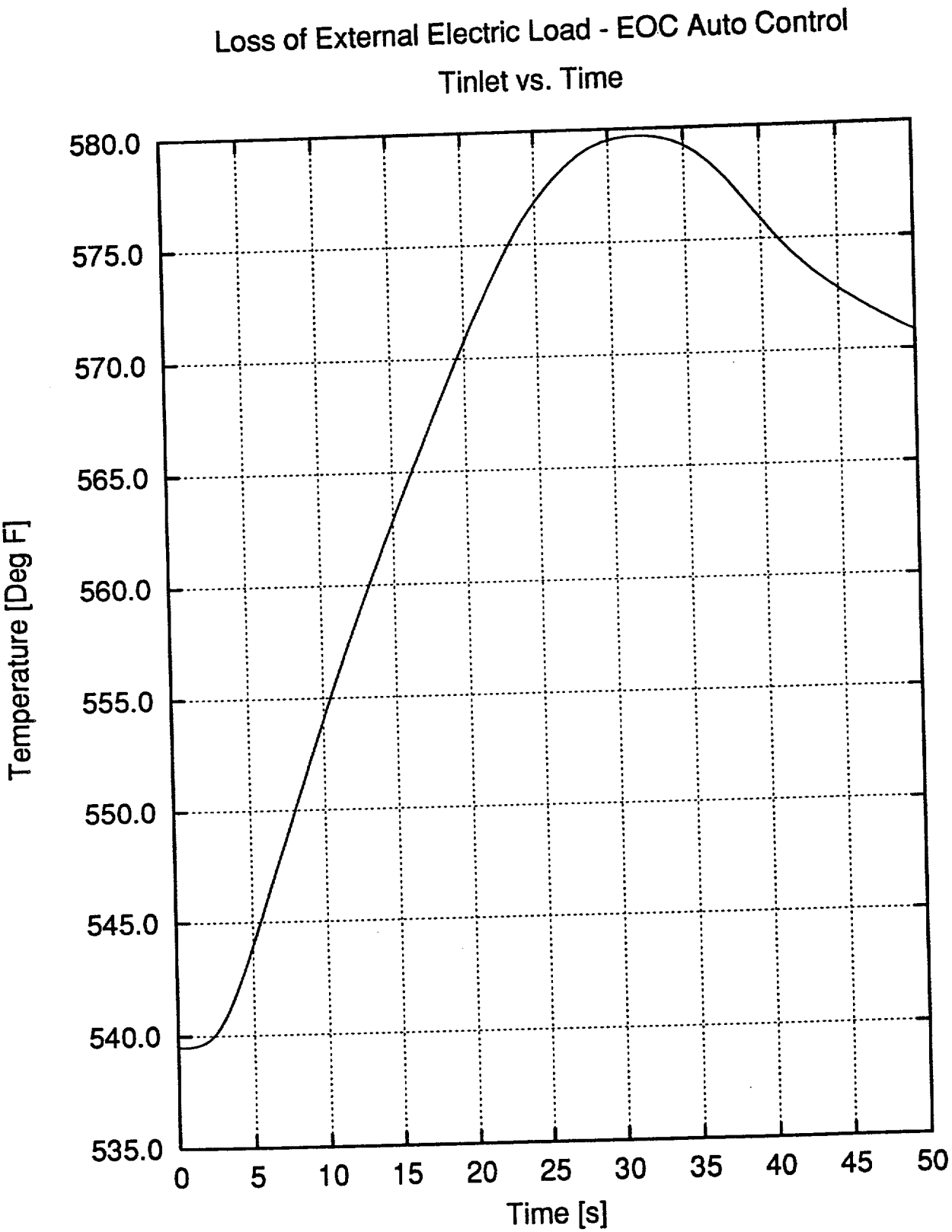
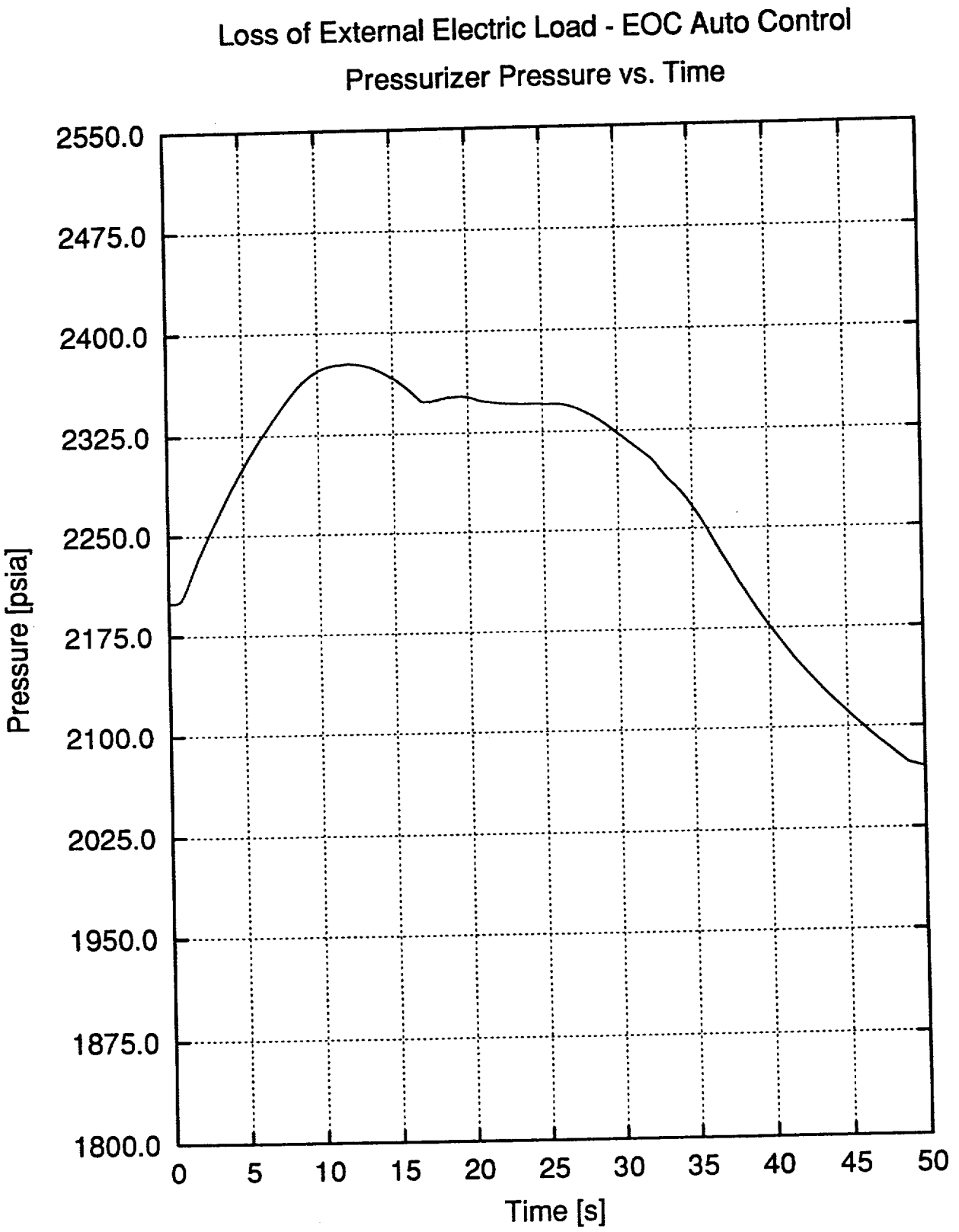


Figure 14.1.9-7



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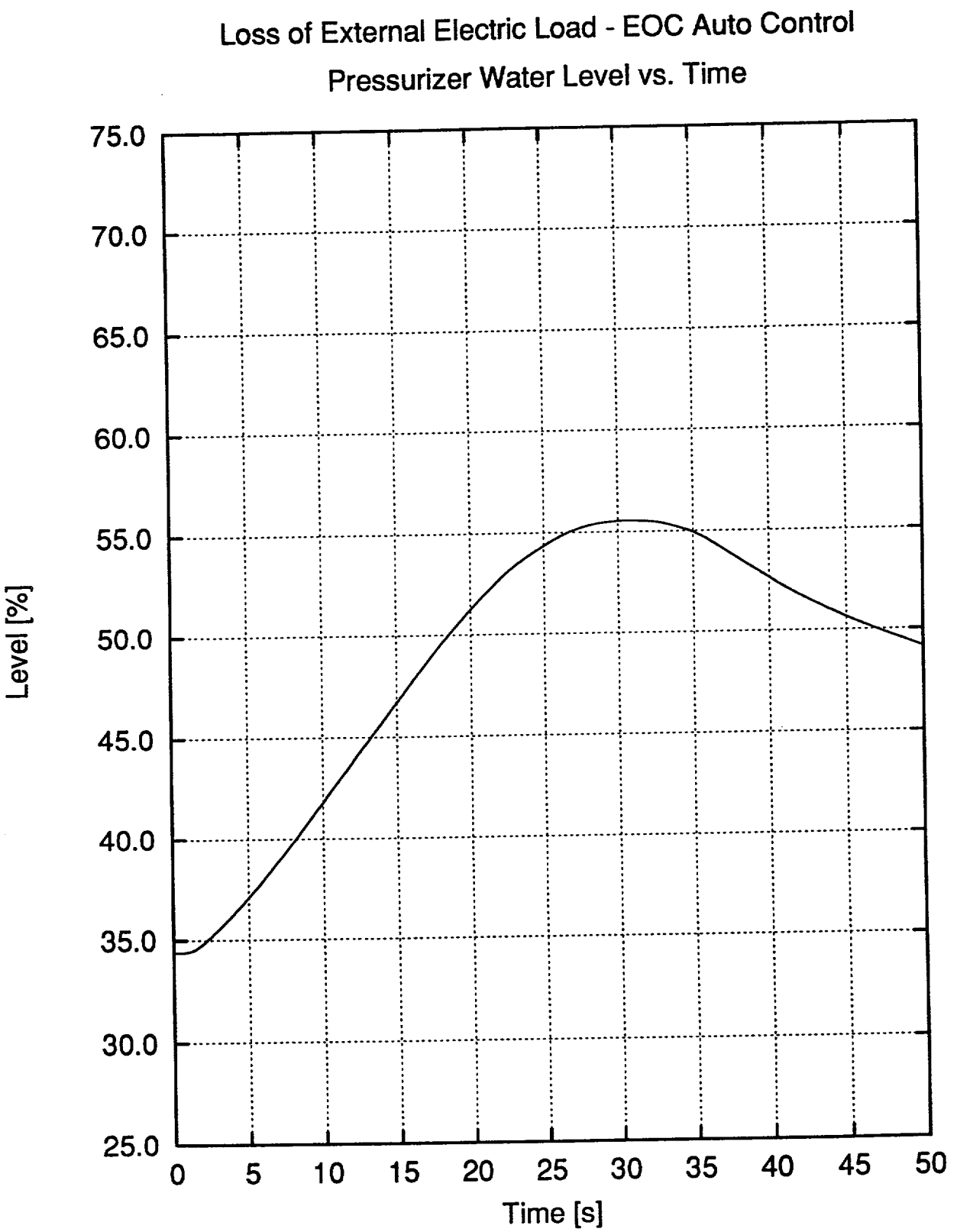
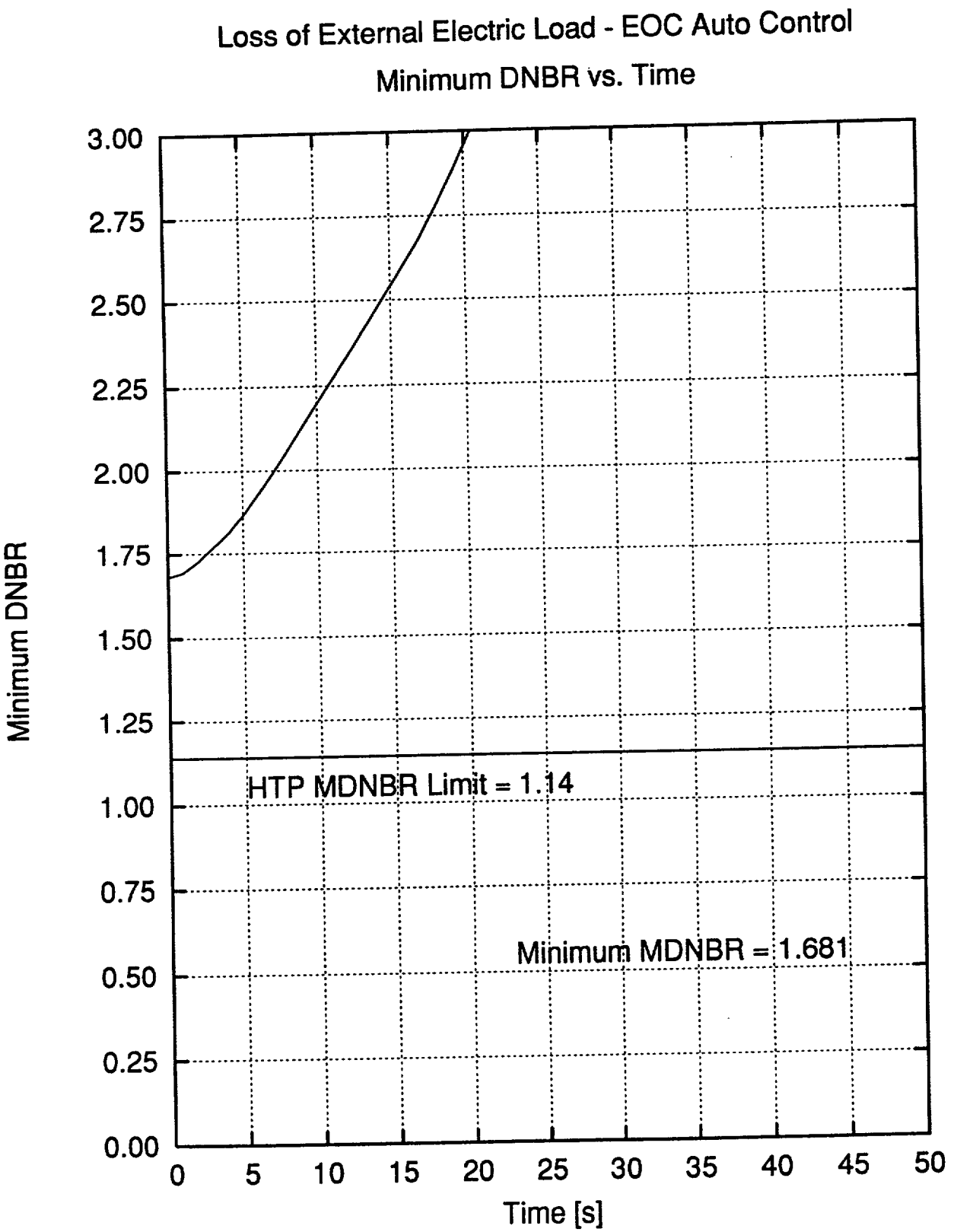


Figure 14.1.9-9



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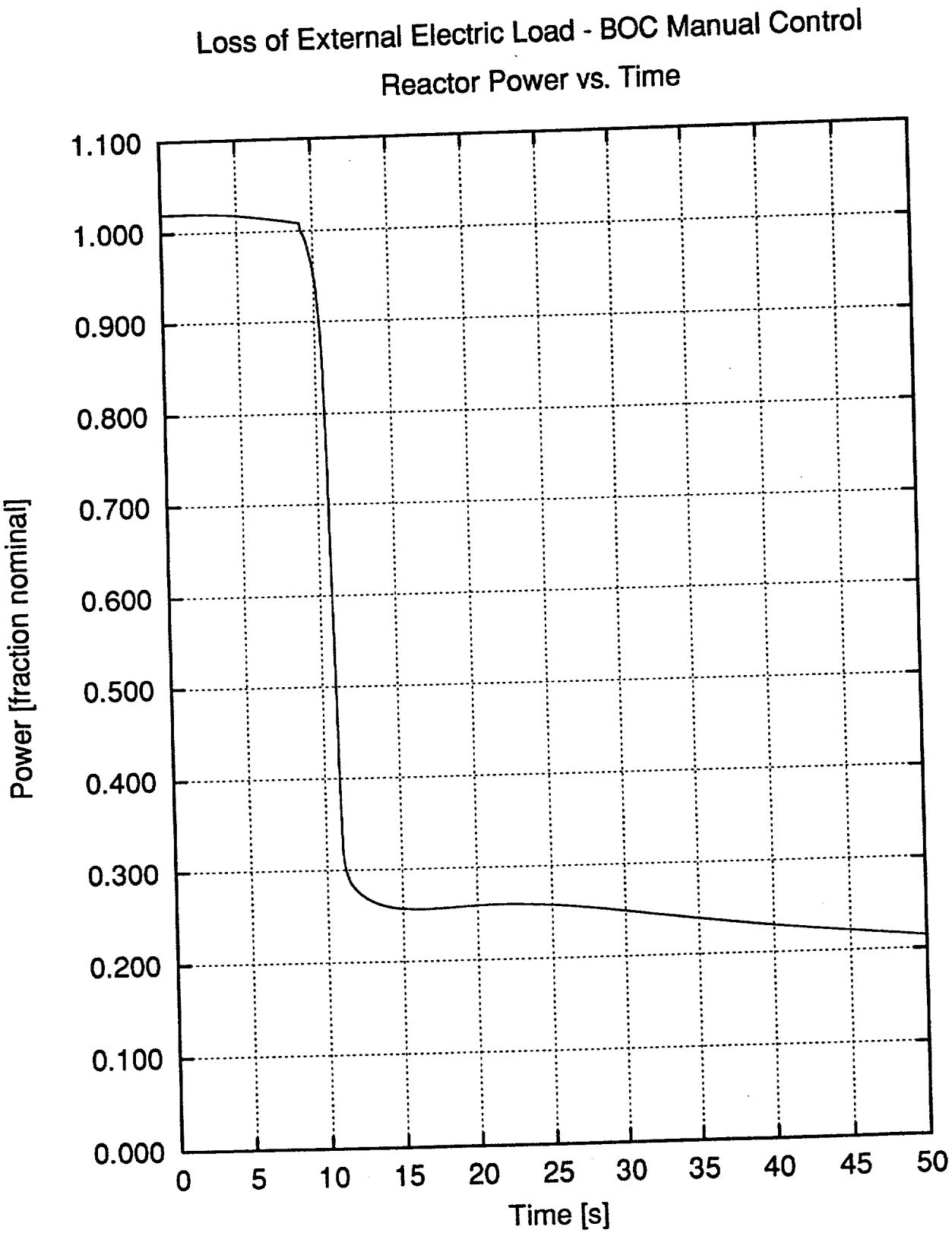


Figure 14.1.9-11

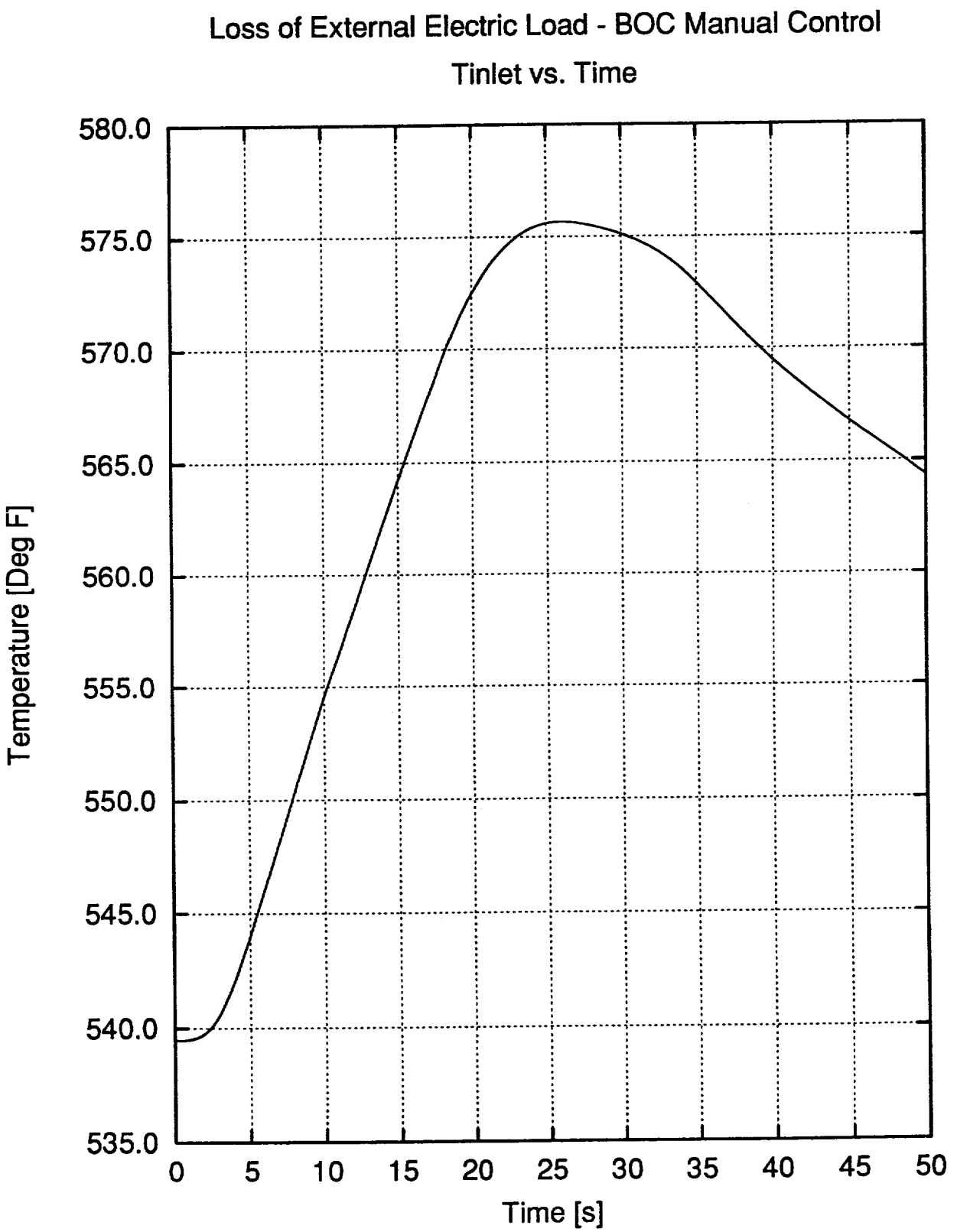


Figure 14.1.9-12

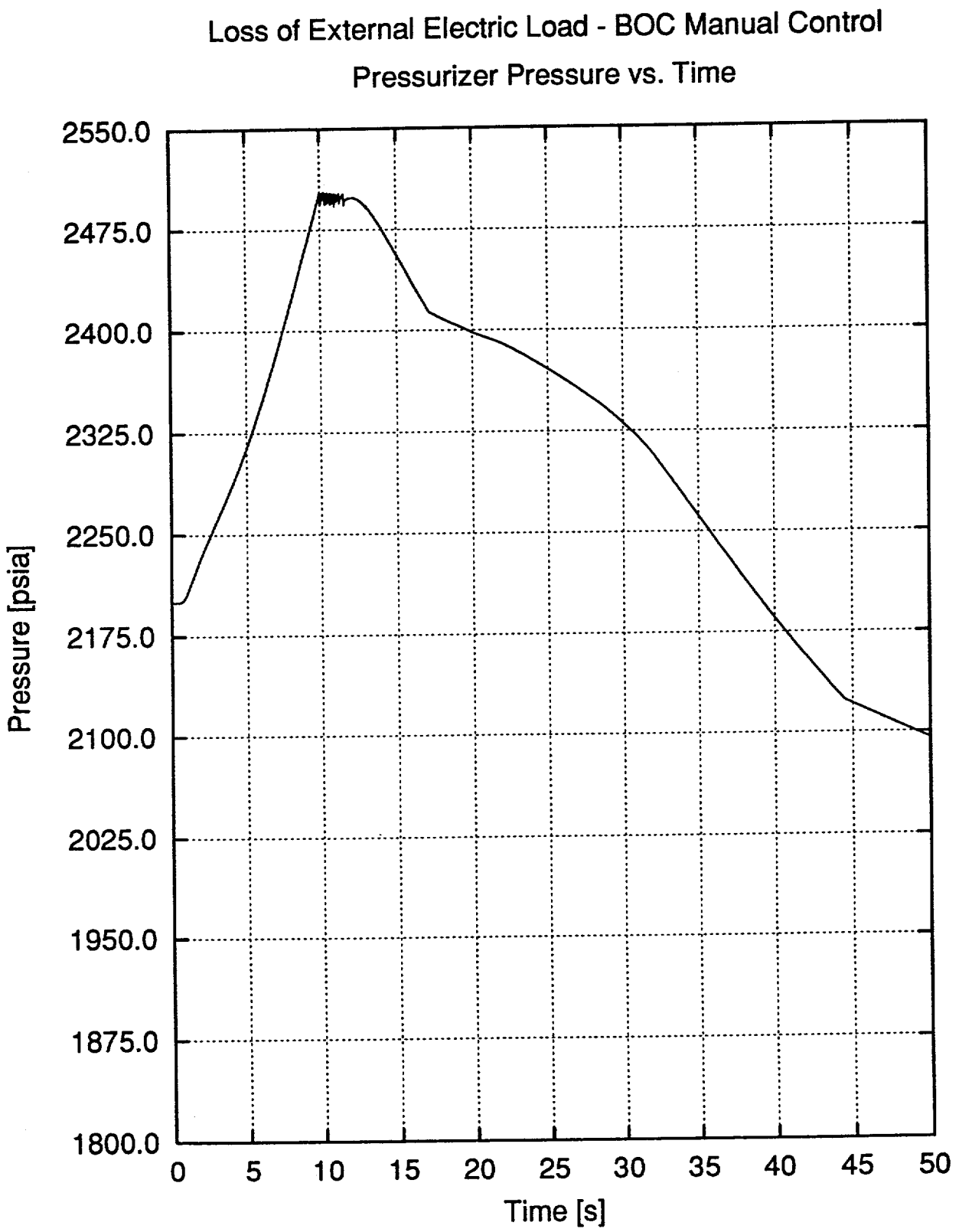
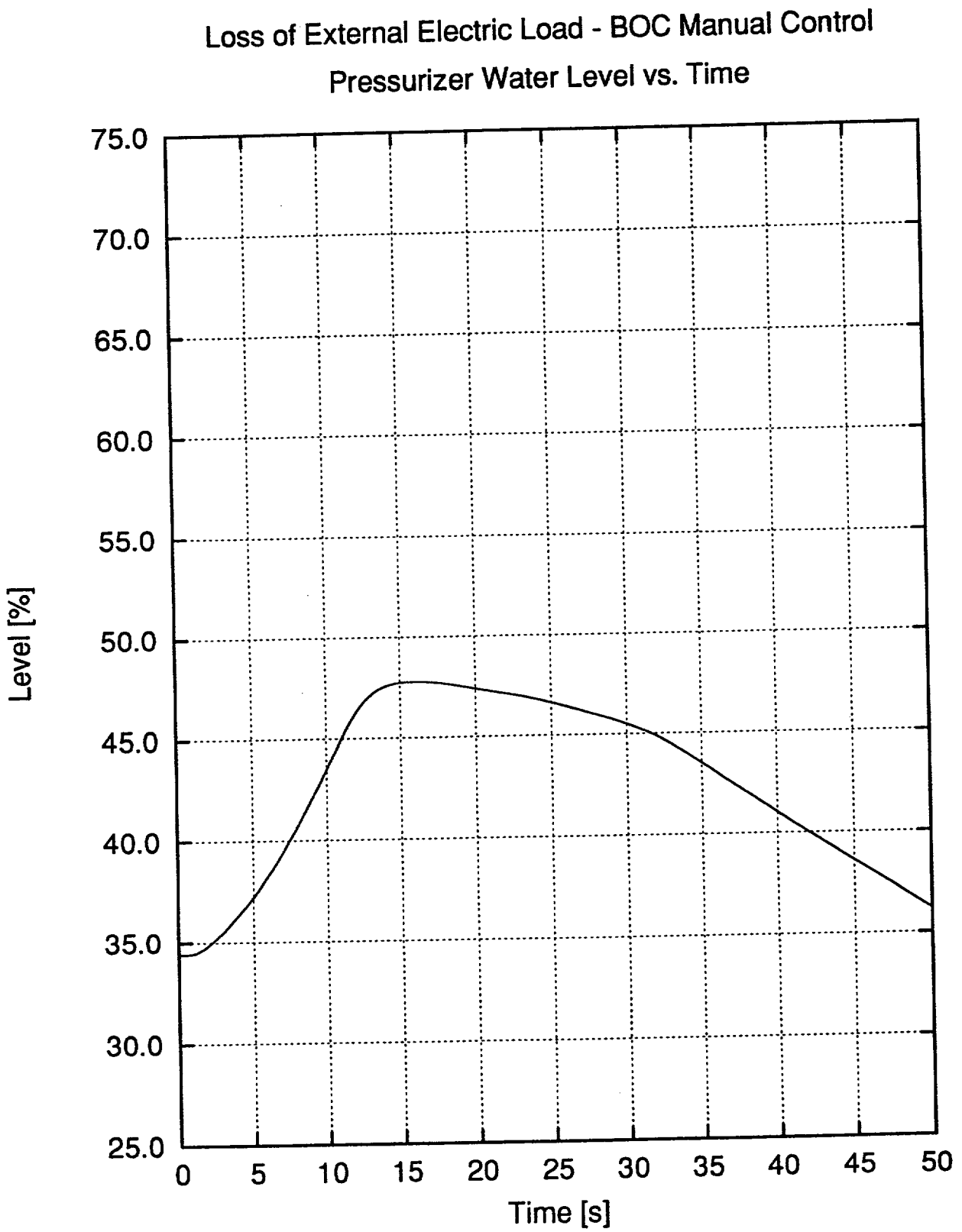


Figure 14.1.9-13



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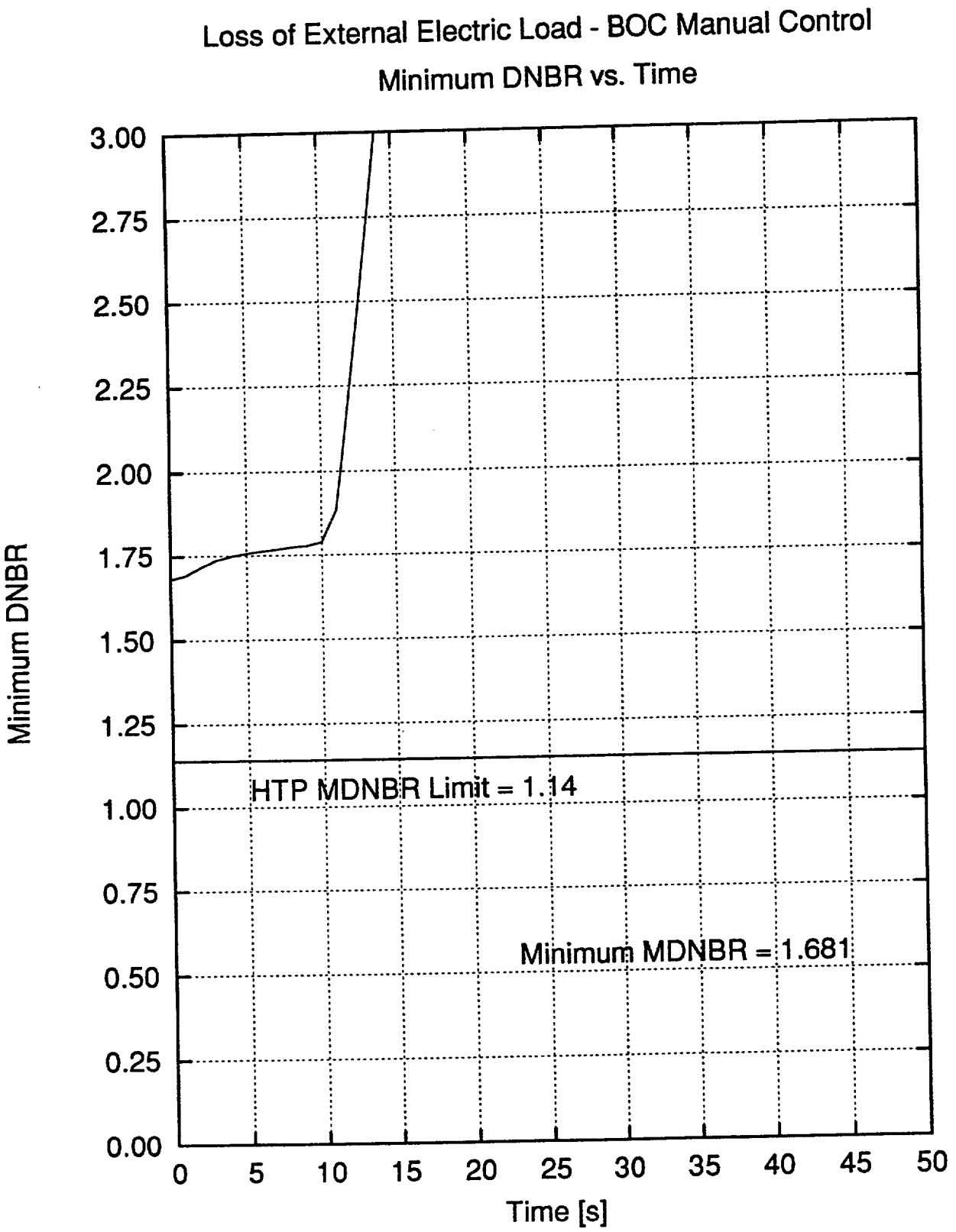

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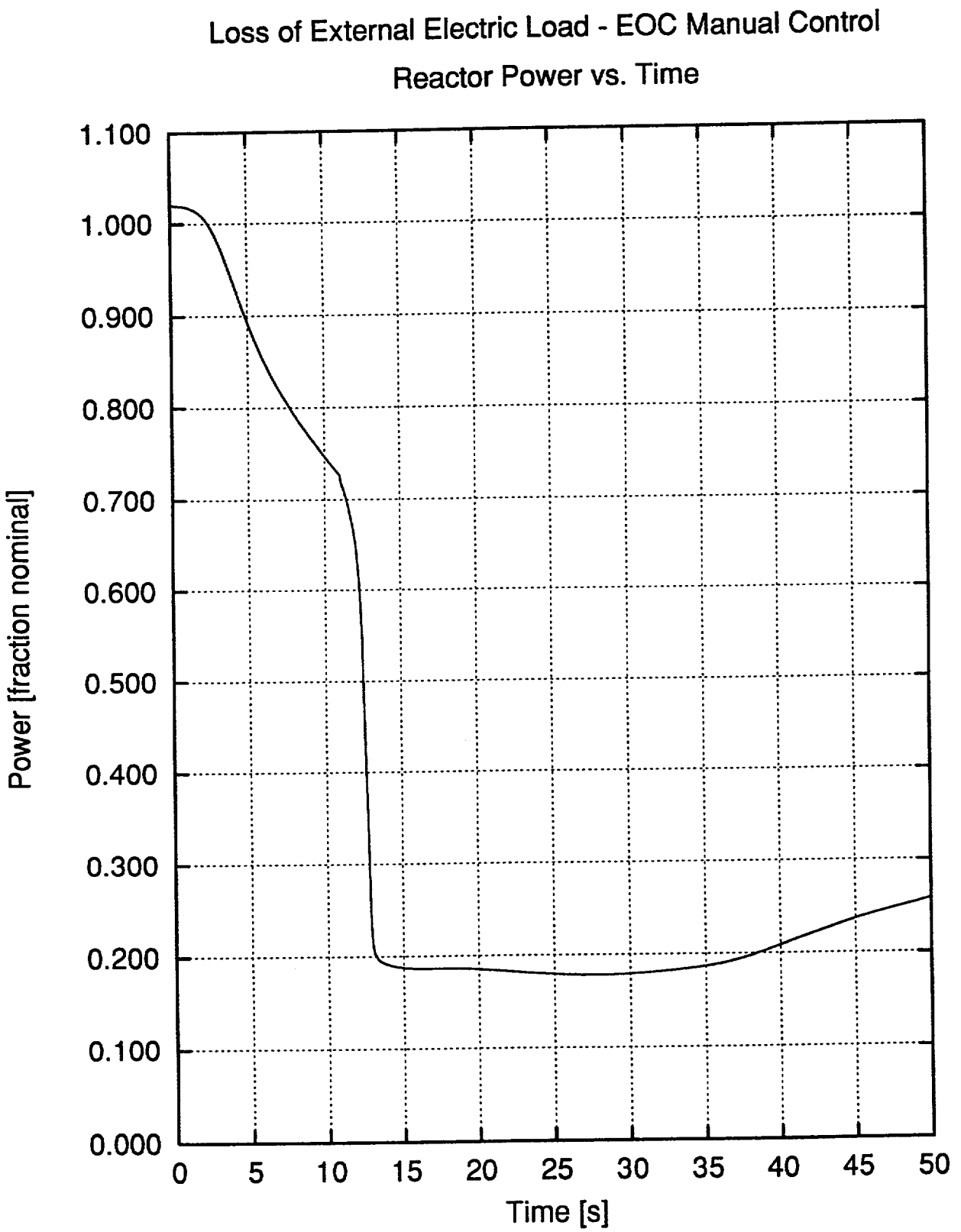

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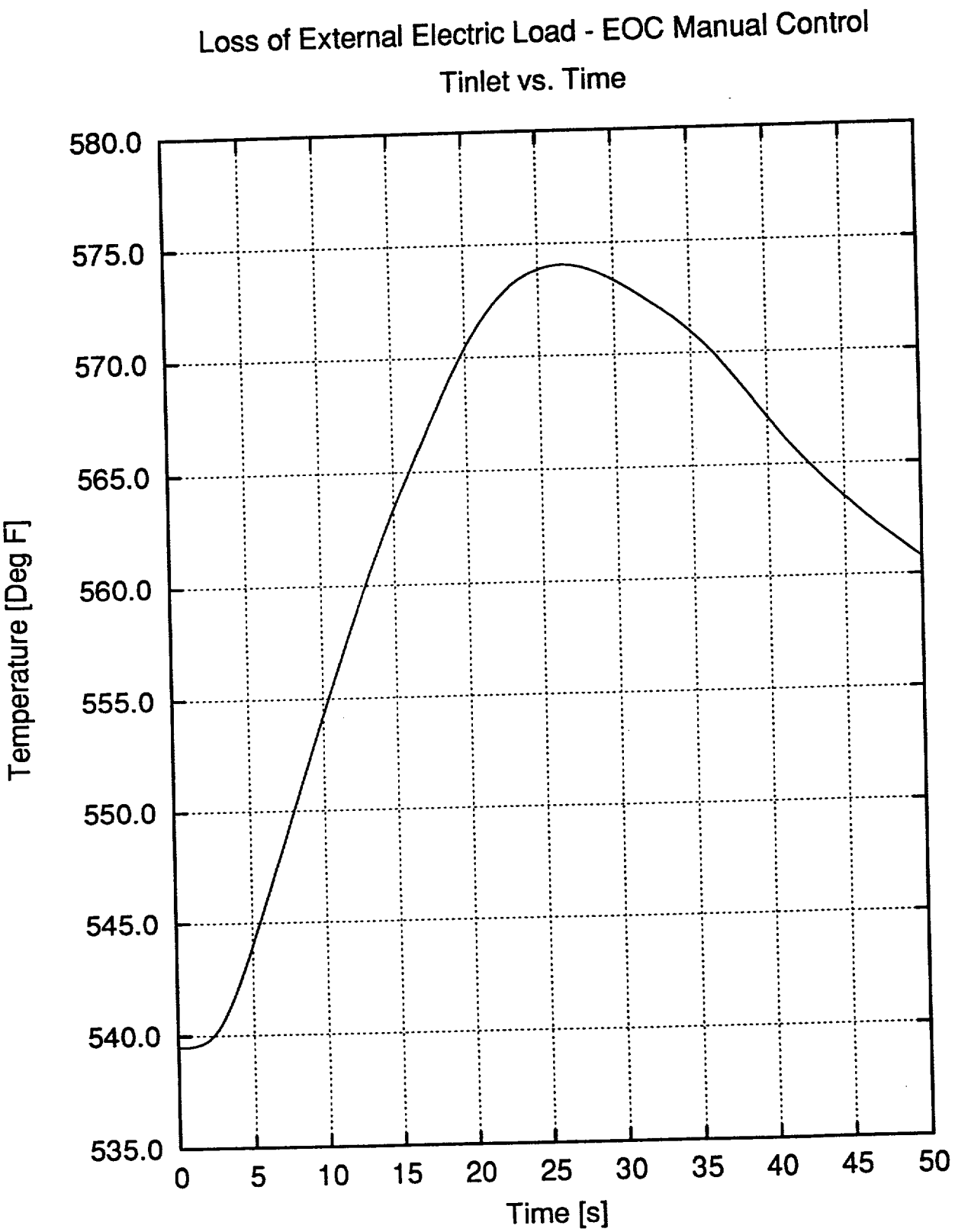

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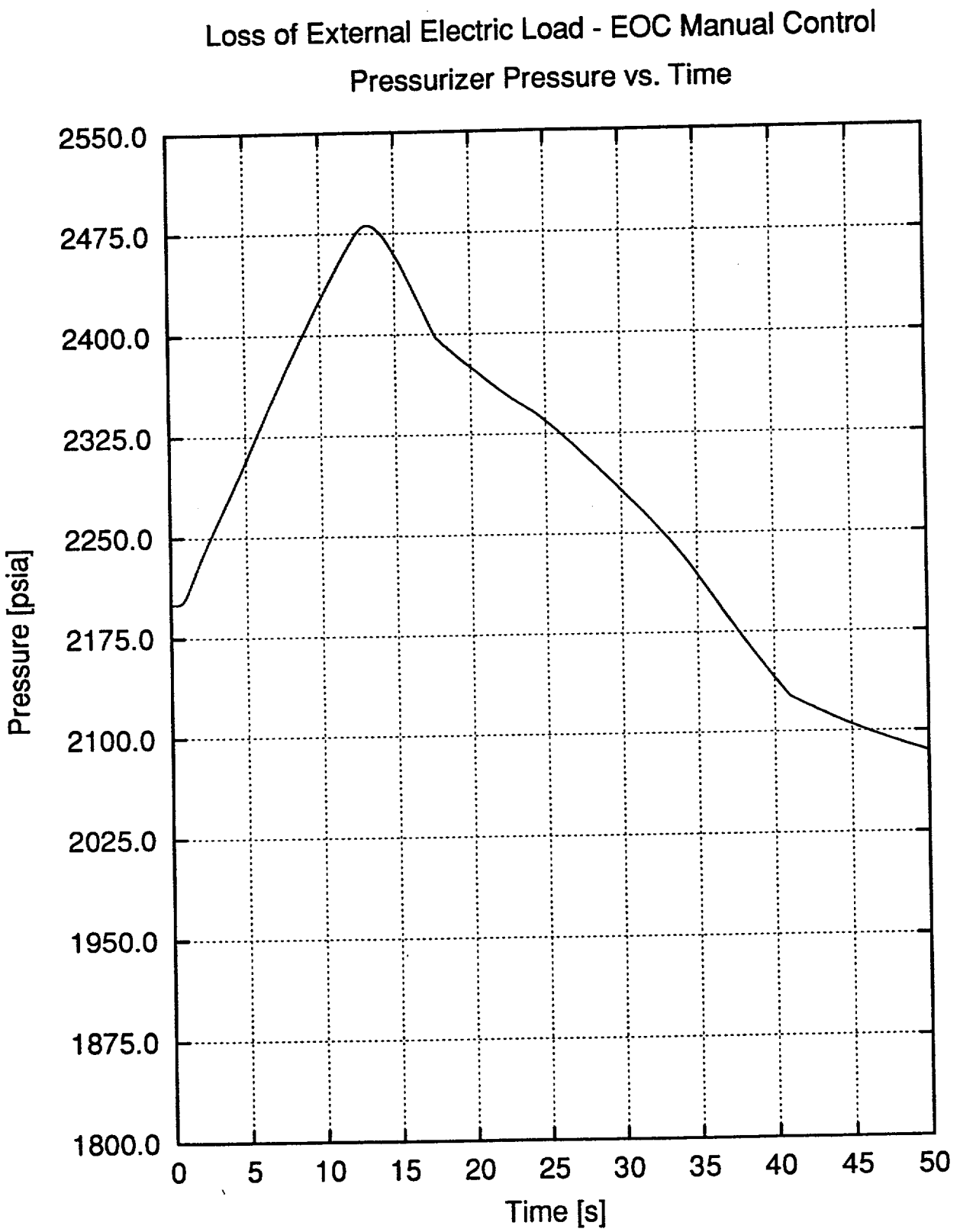

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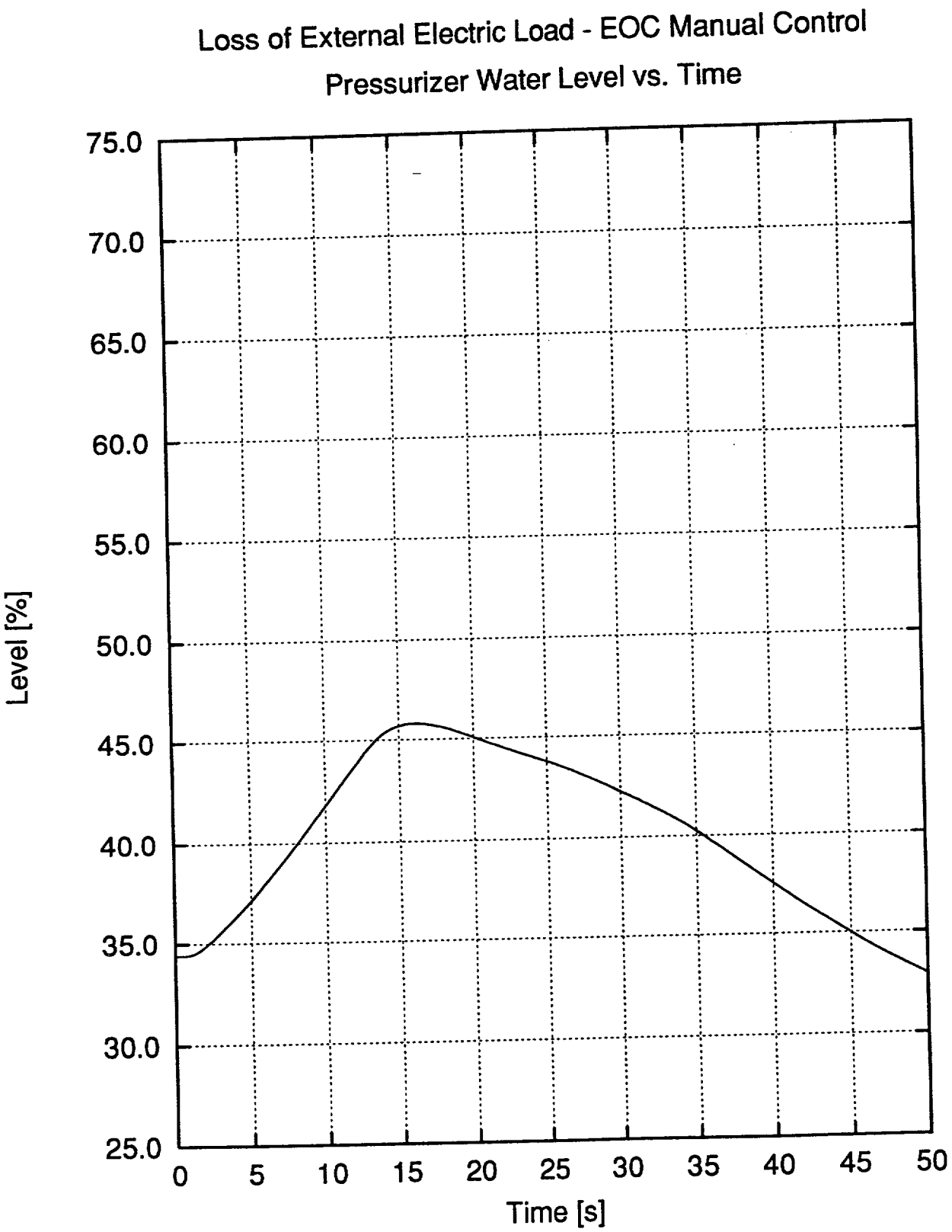

Figure 14.1.9-19

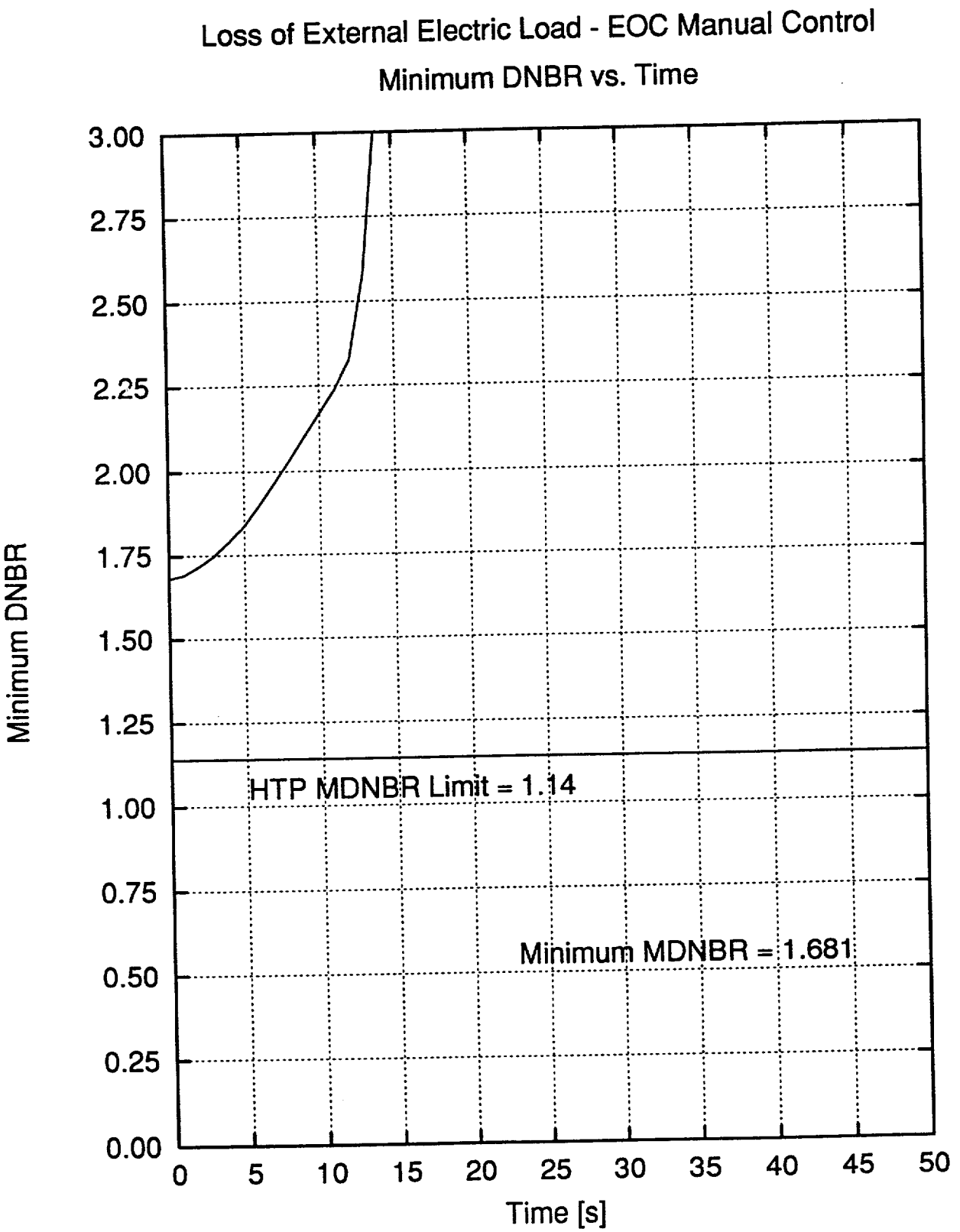

Figure 14.1.9-20

LOSS OF NORMAL FEEDWATER

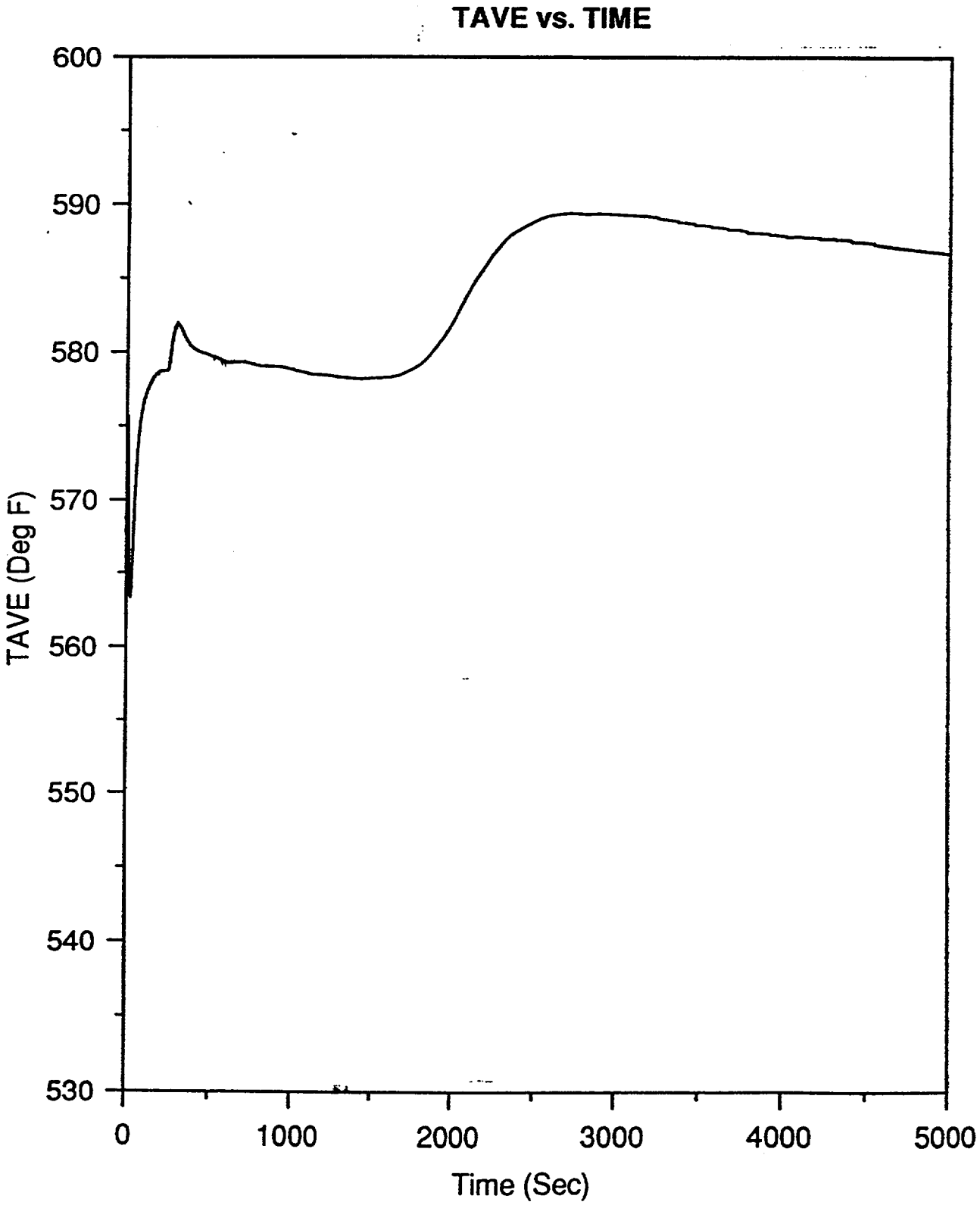

FIGURE 14.1.10-1

LOSS OF NORMAL FEEDWATER

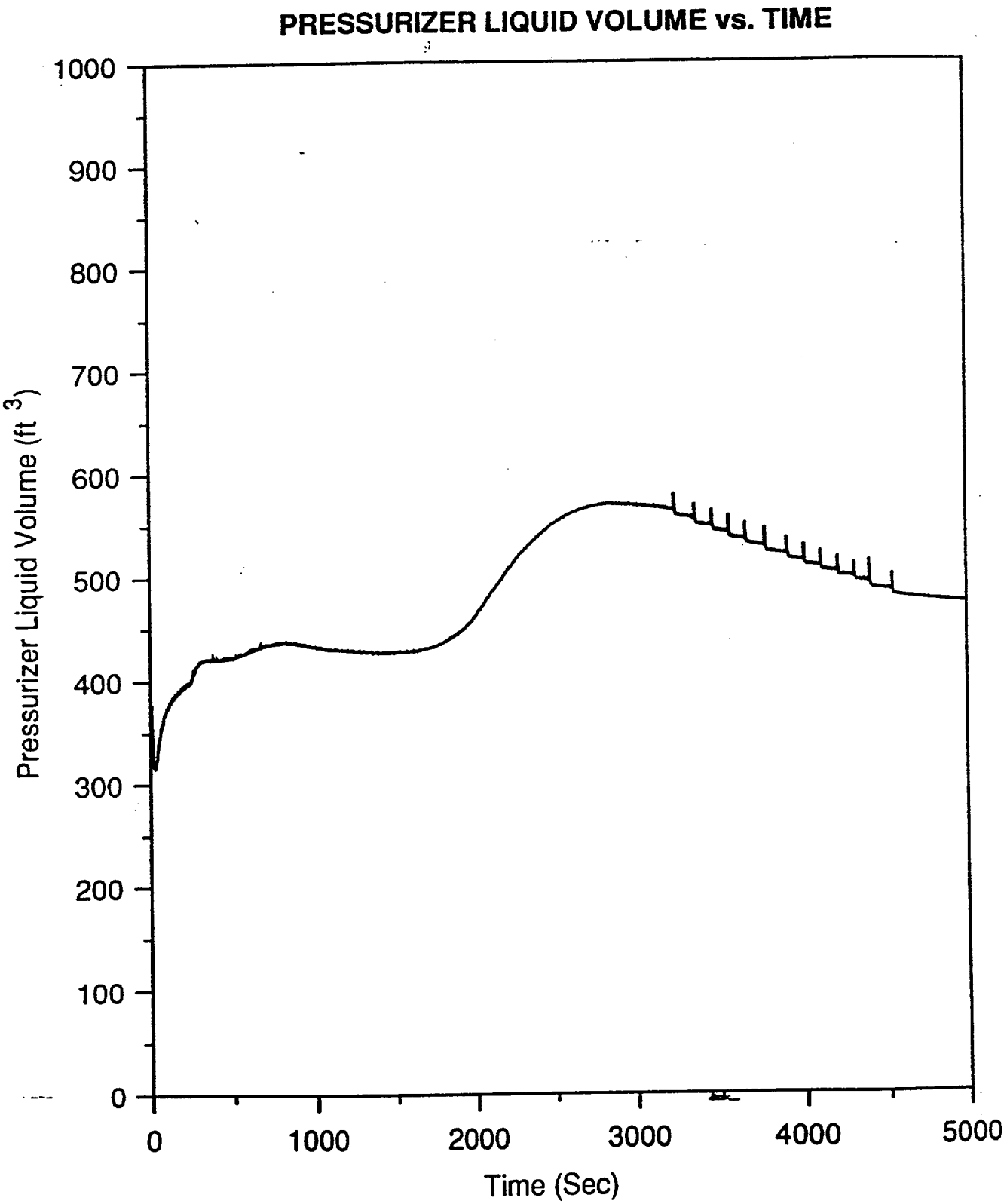

FIGURE 14.1.10-2

LOSS OF NORMAL FEEDWATER

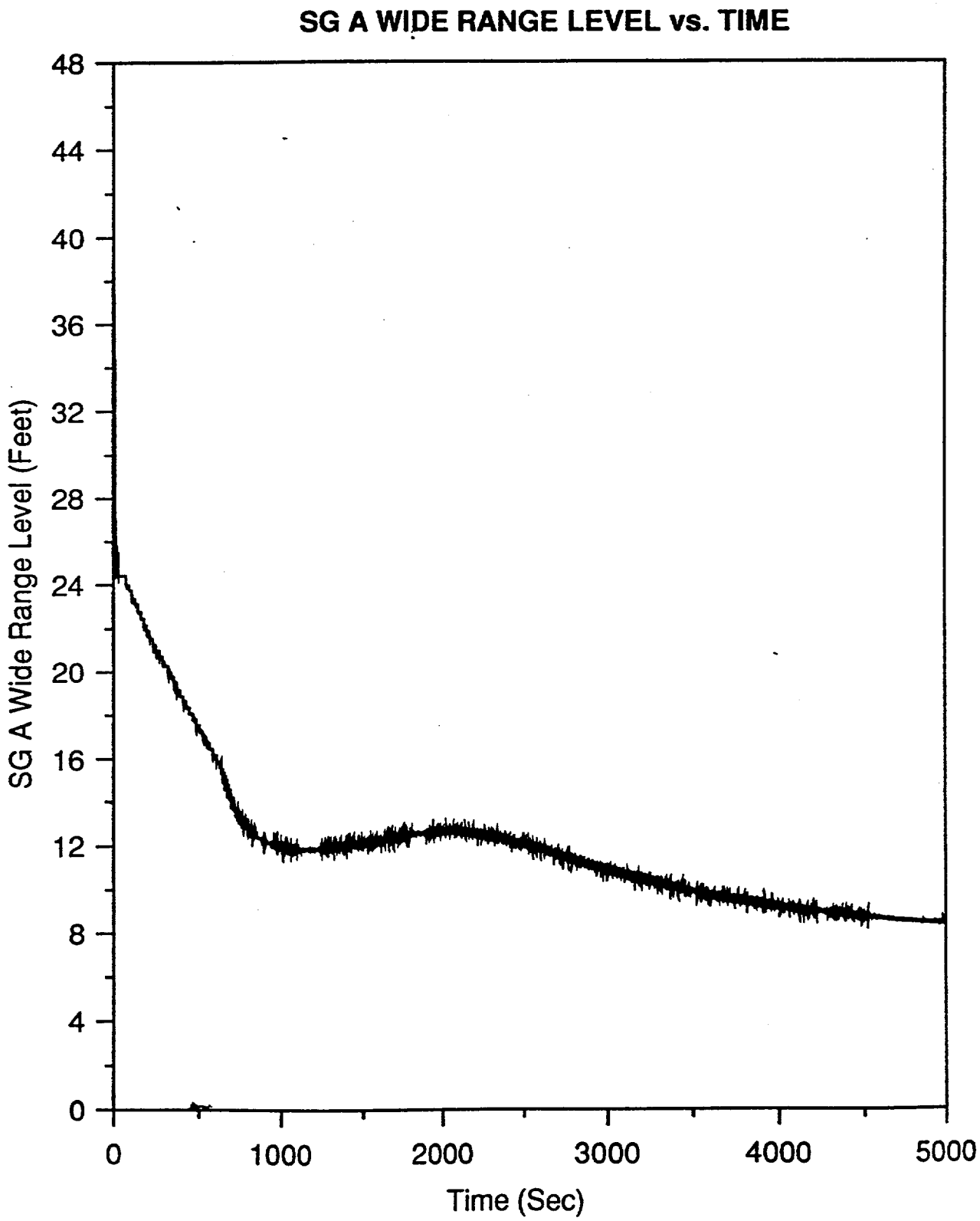

FIGURE 14.1.10-3

LOSS OF NORMAL FEEDWATER

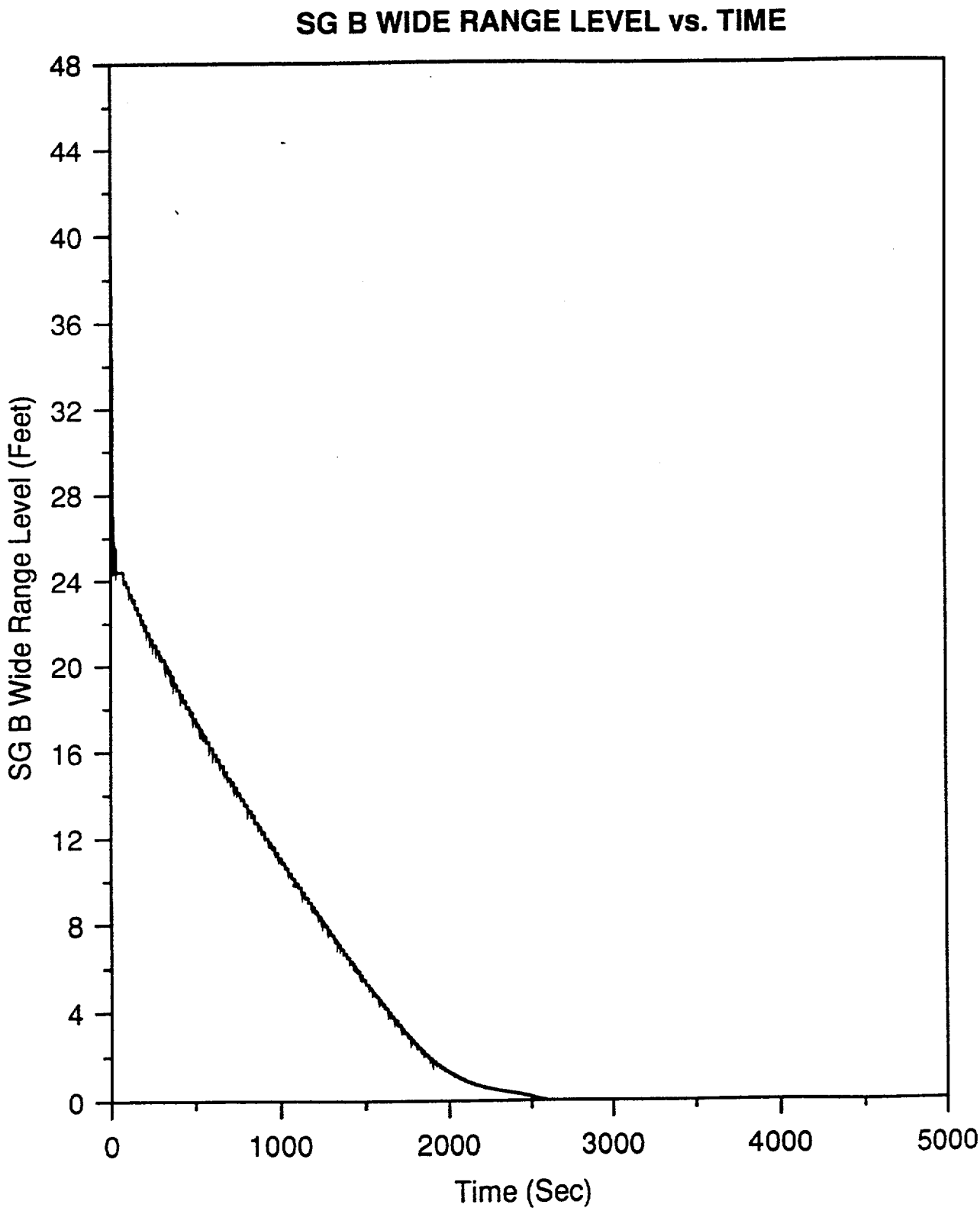

FIGURE 14.1.10-4

LOSS OF NORMAL FEEDWATER

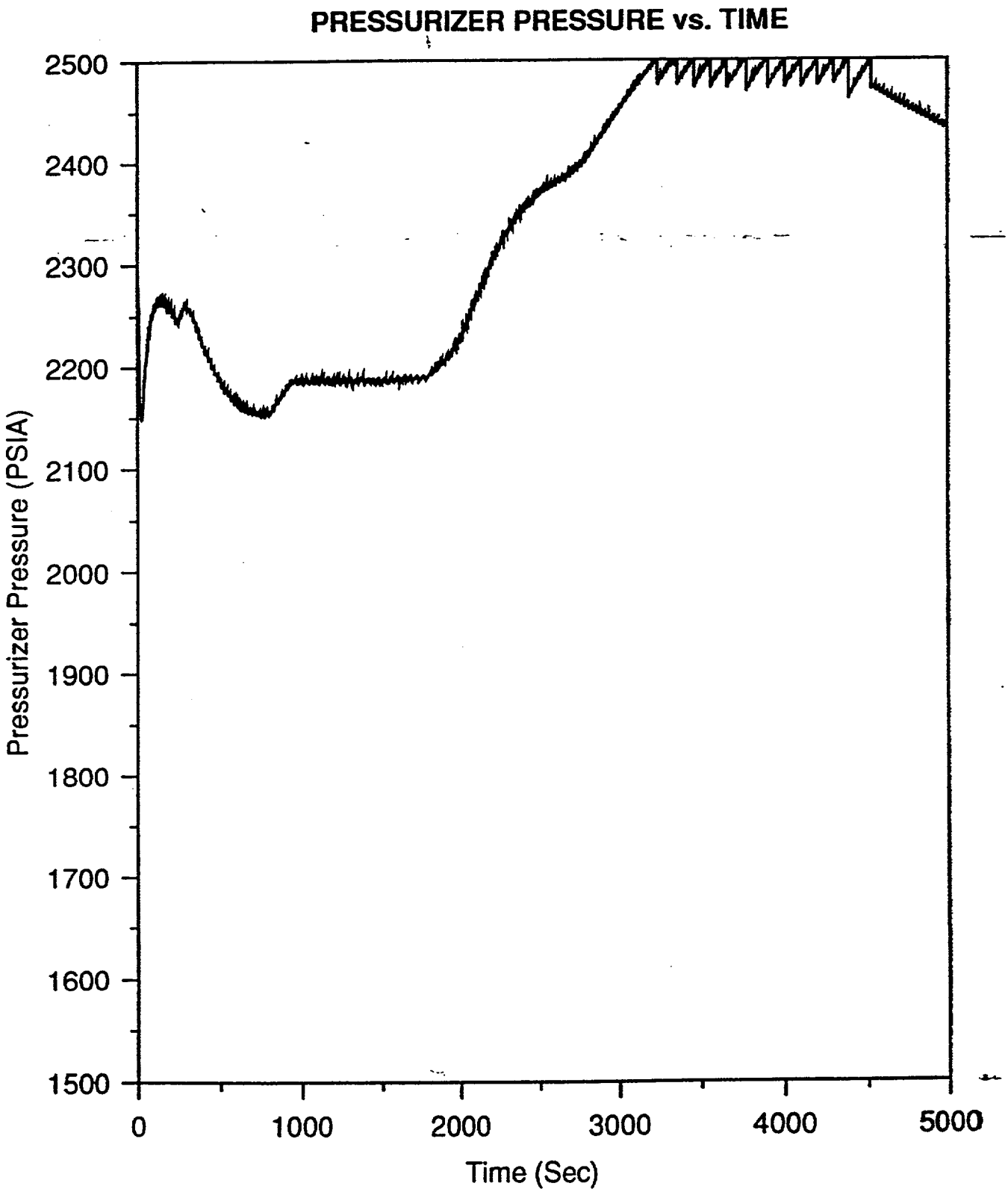

FIGURE 14.1.10-5

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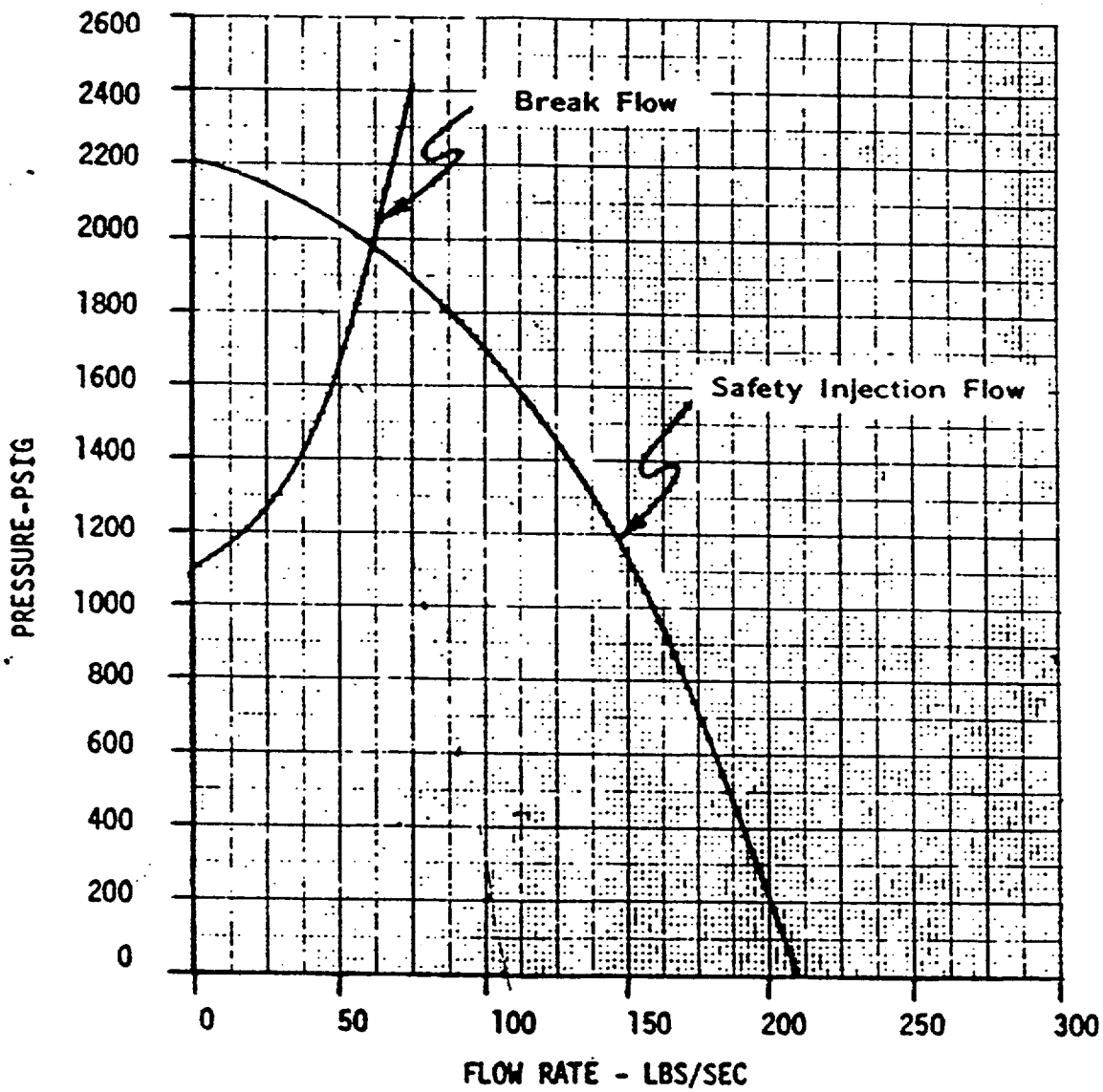

BREAK FLOW AND SAFETY INJECTION FLOW (TWO PUMPS)
VS REACTOR COOLANT PRESSURE

FIGURE 14.2.4-1

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Main Steam Line Break Variation of Reactivity with Core Temperature at 1000 psia for EOL Rodded Core with One Stuck Rod (Zero Power)

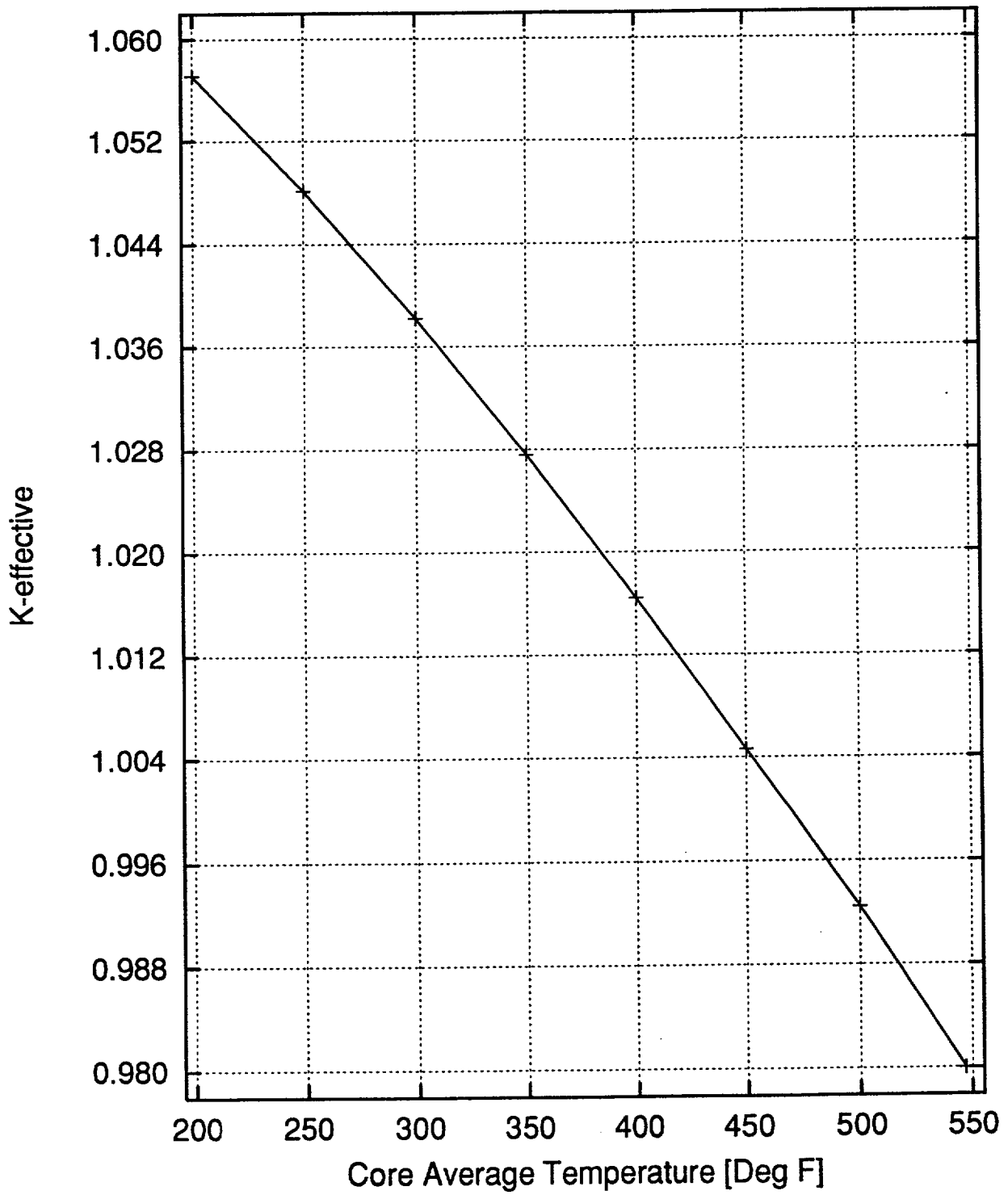

Figure 14.2.5-1

MAIN STEAM LINE BREAK

VARIATION OF REACTIVITY WITH POWER AT CONSTANT CORE AVERAGE TEMPERATURE

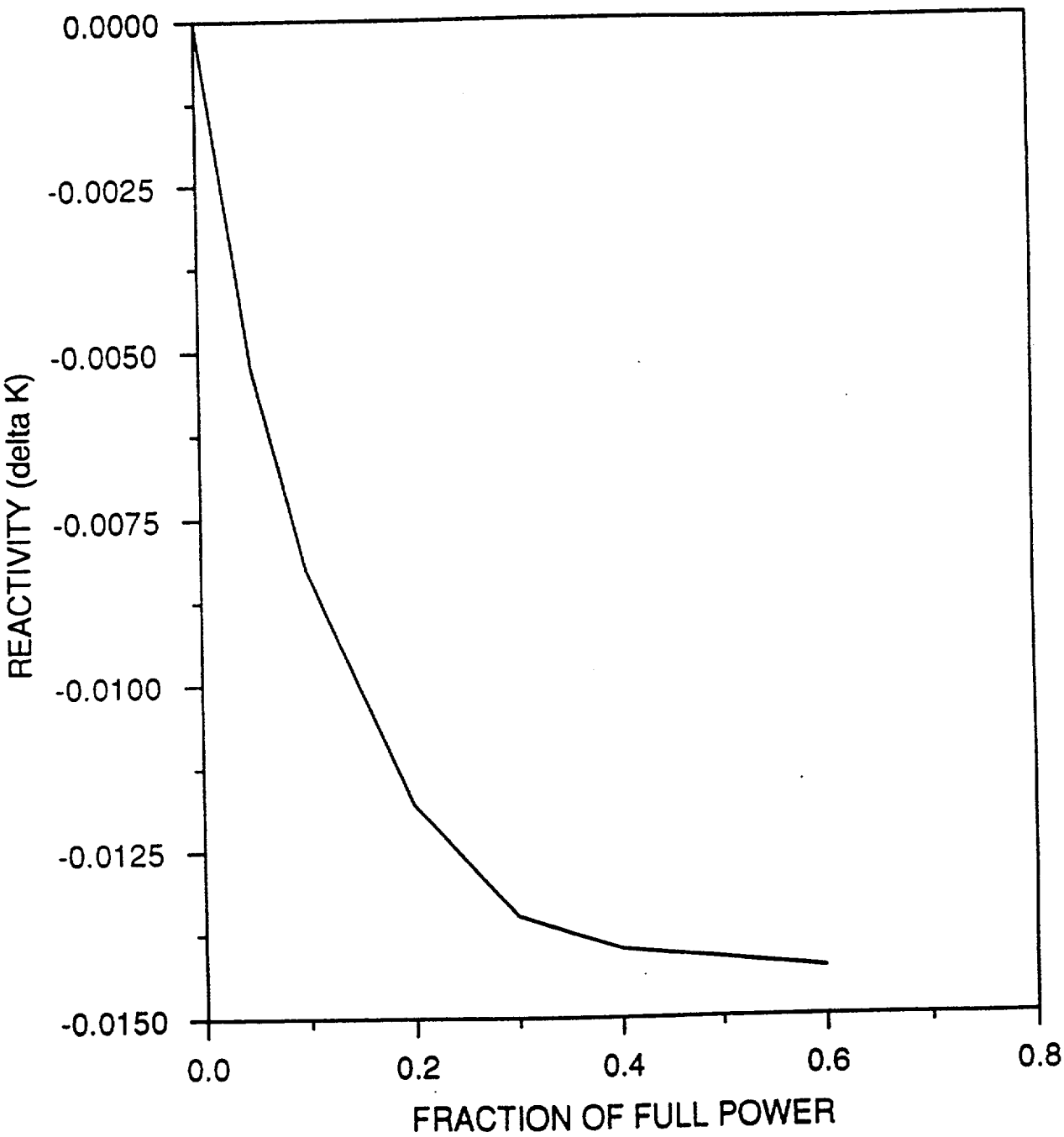

FIGURE 14.2.5-2

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MAIN STEAM LINE BREAK

SAFETY INJECTION FLOW RATE VS REACTOR COOLANT PRESSURE

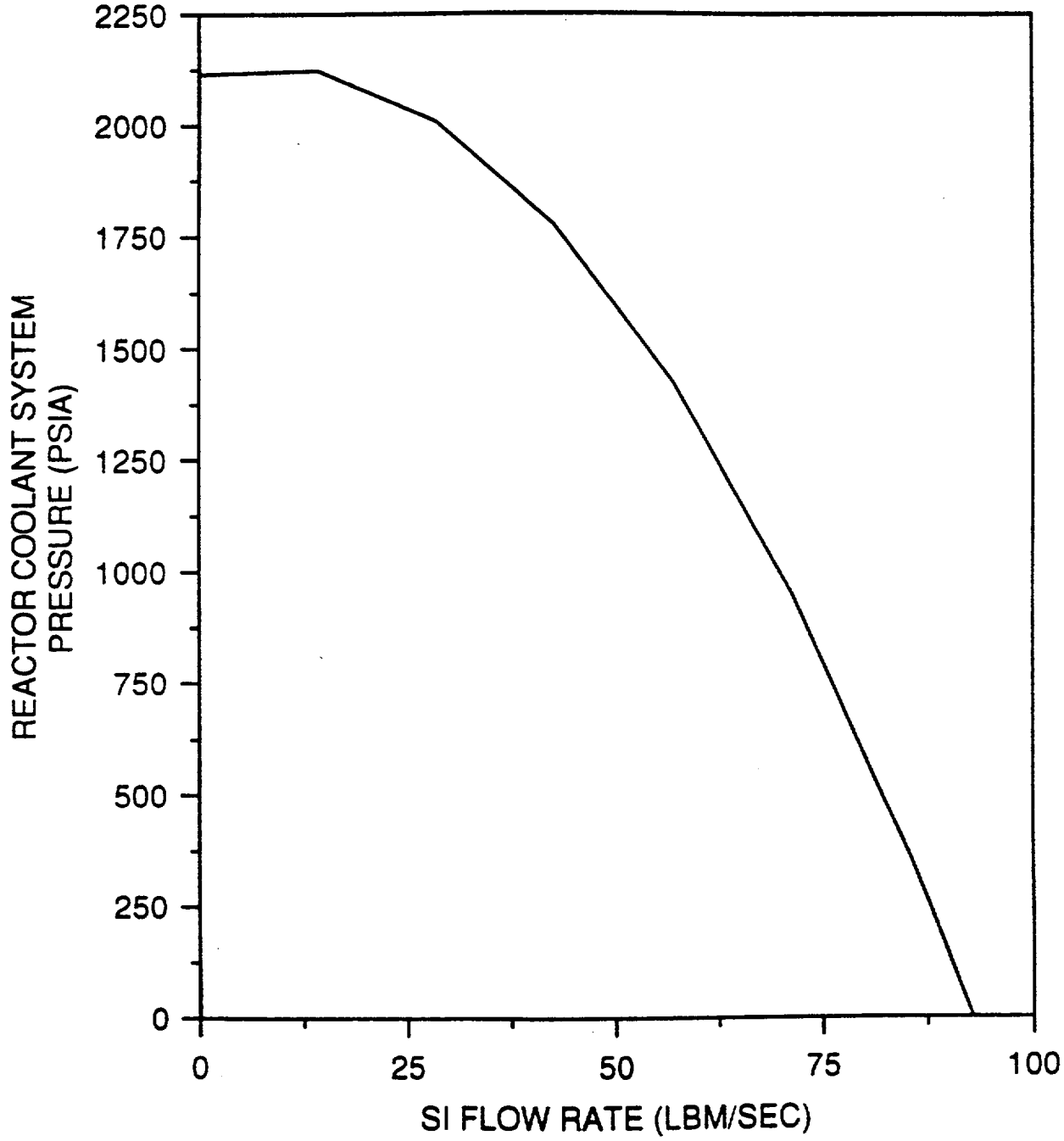

FIGURE 14.2.5-3

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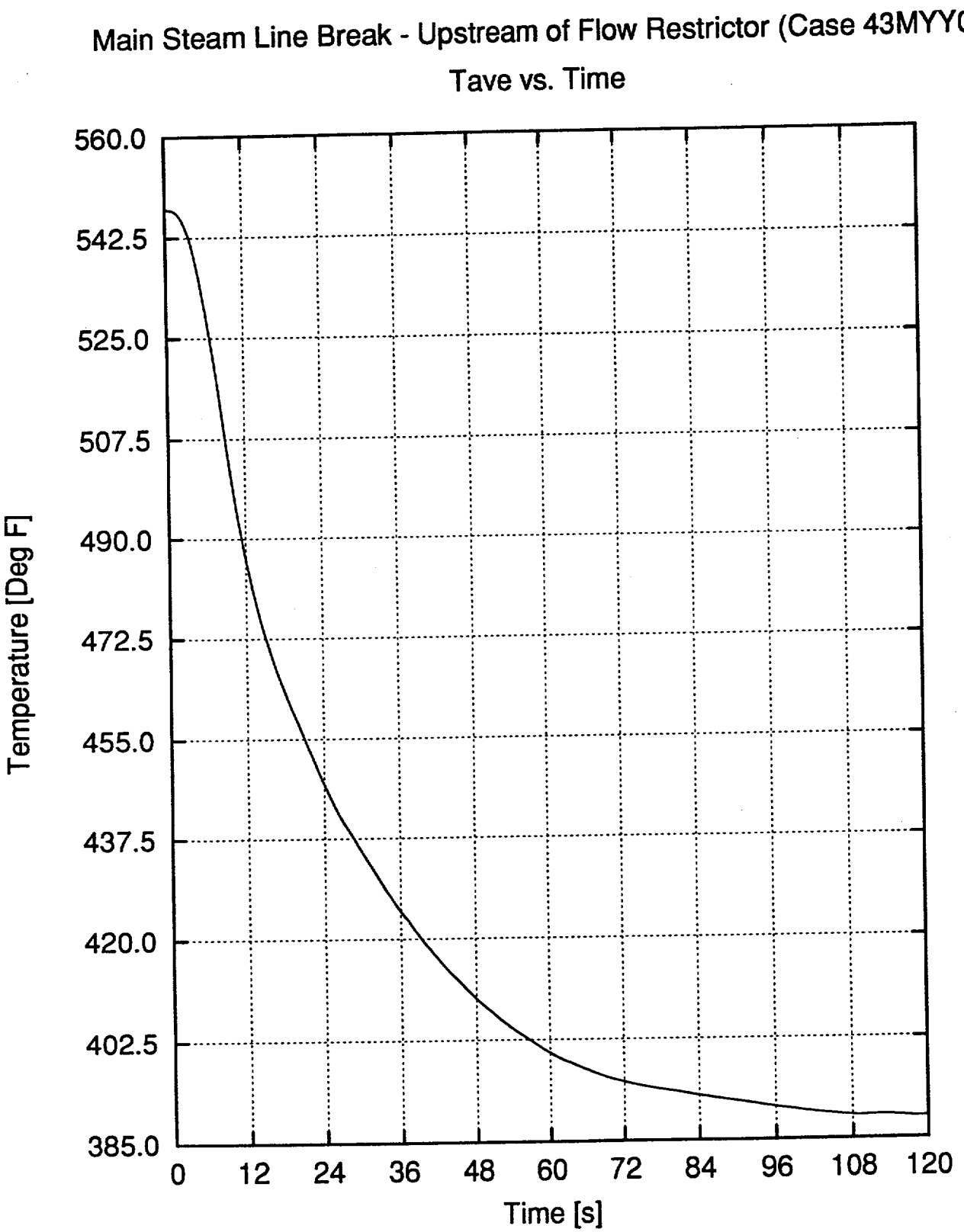

Figure 14.2.5-4

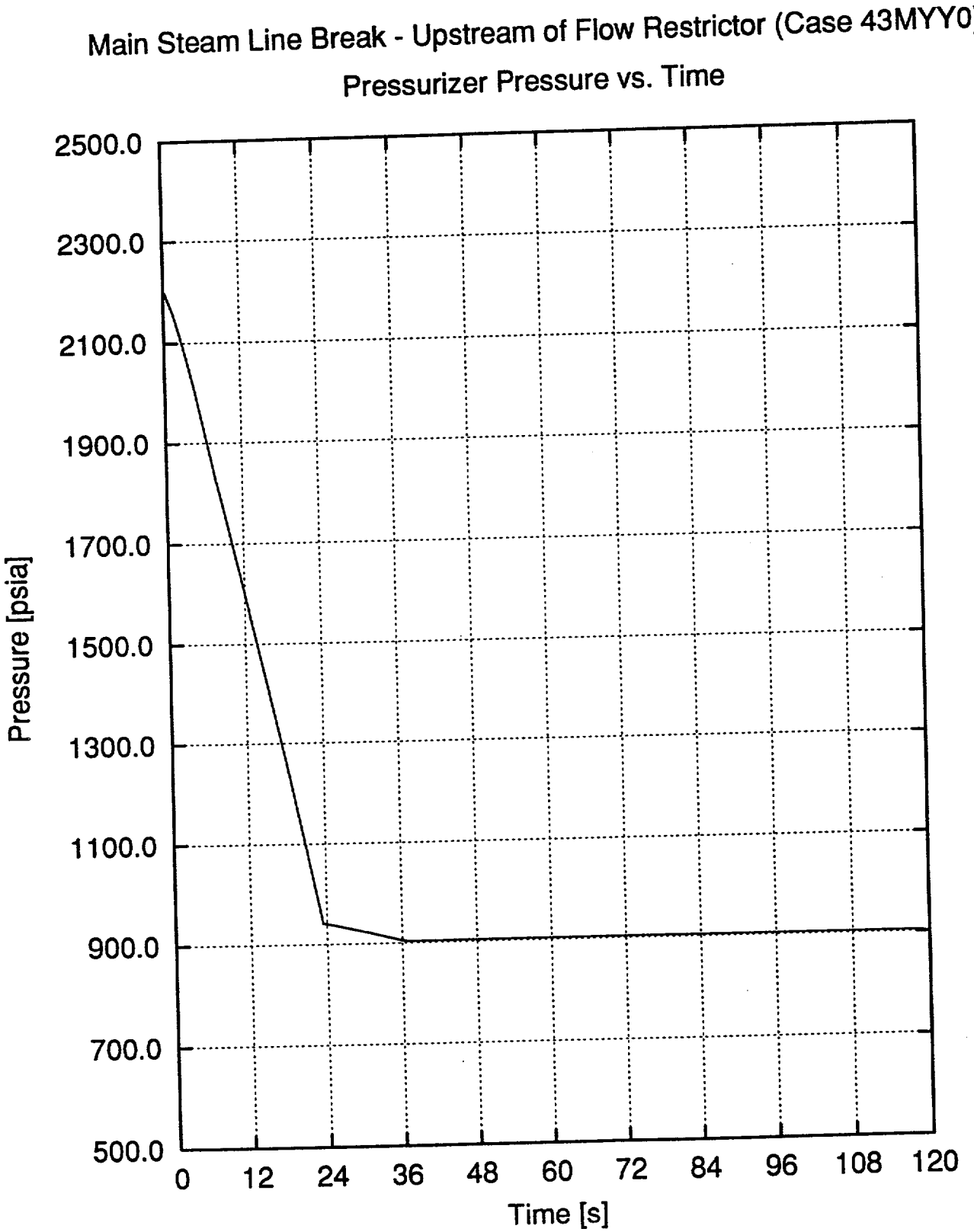

Figure 14.2.5-5

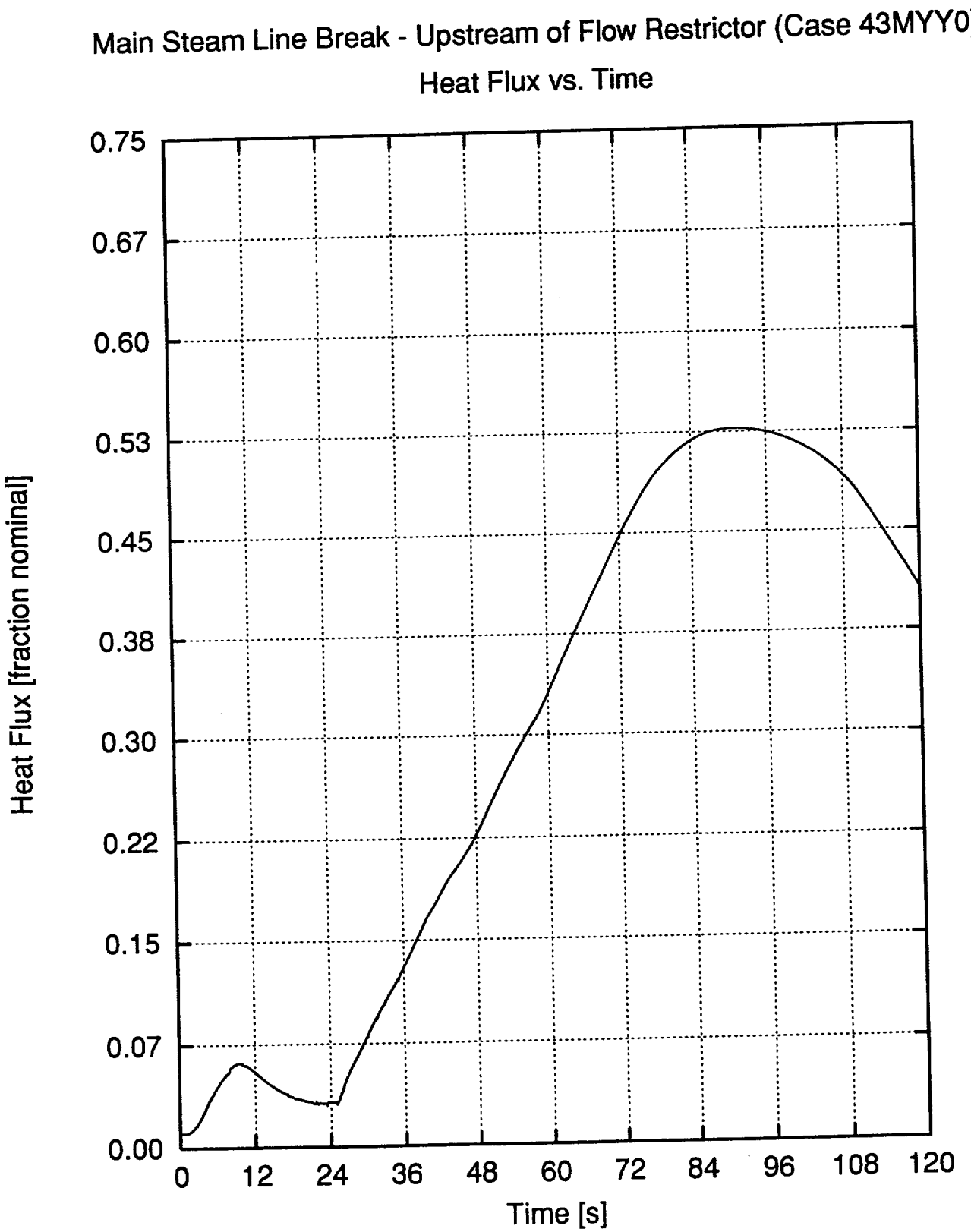

Figure 14.2.5-6

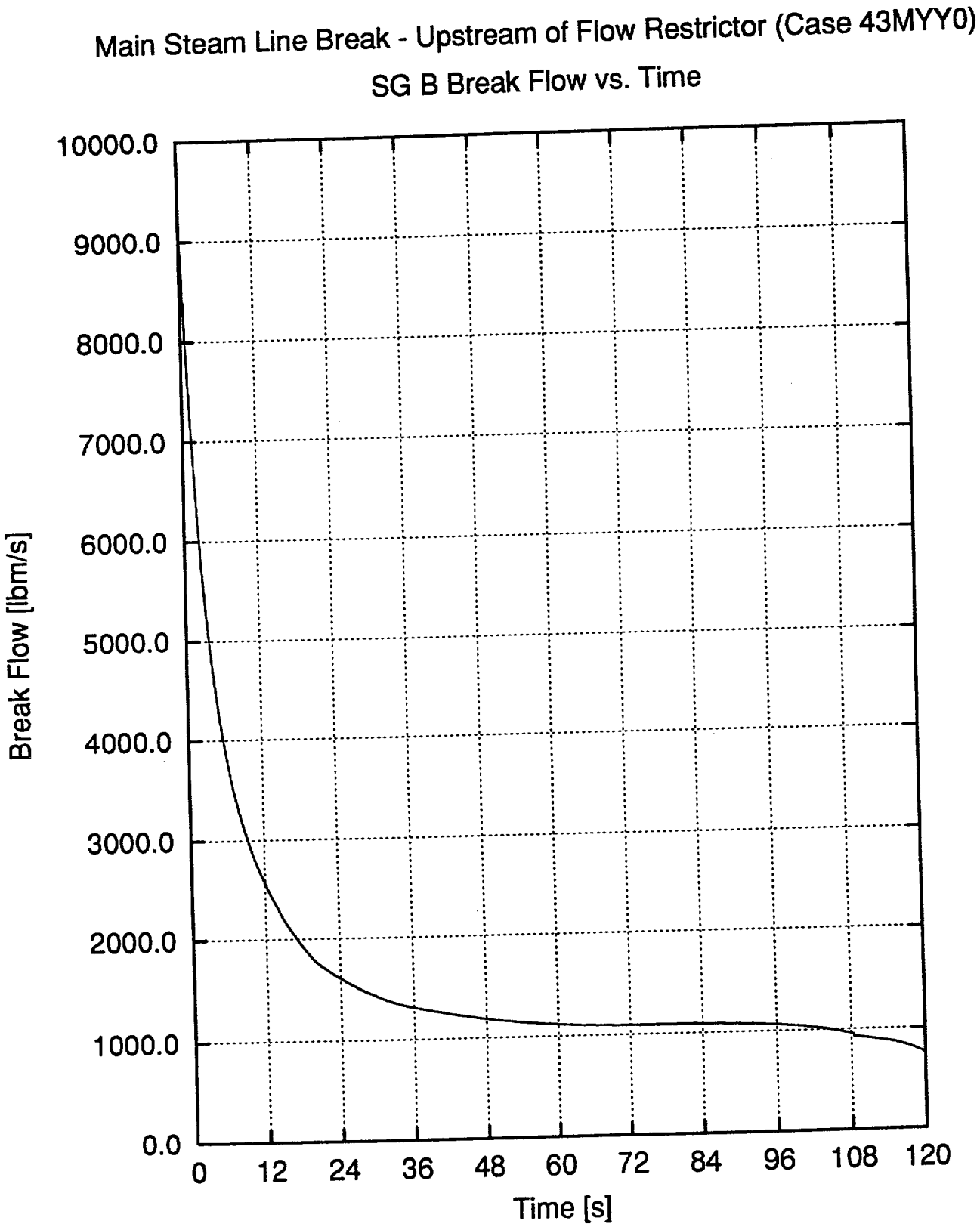

Figure 14.2.5-7

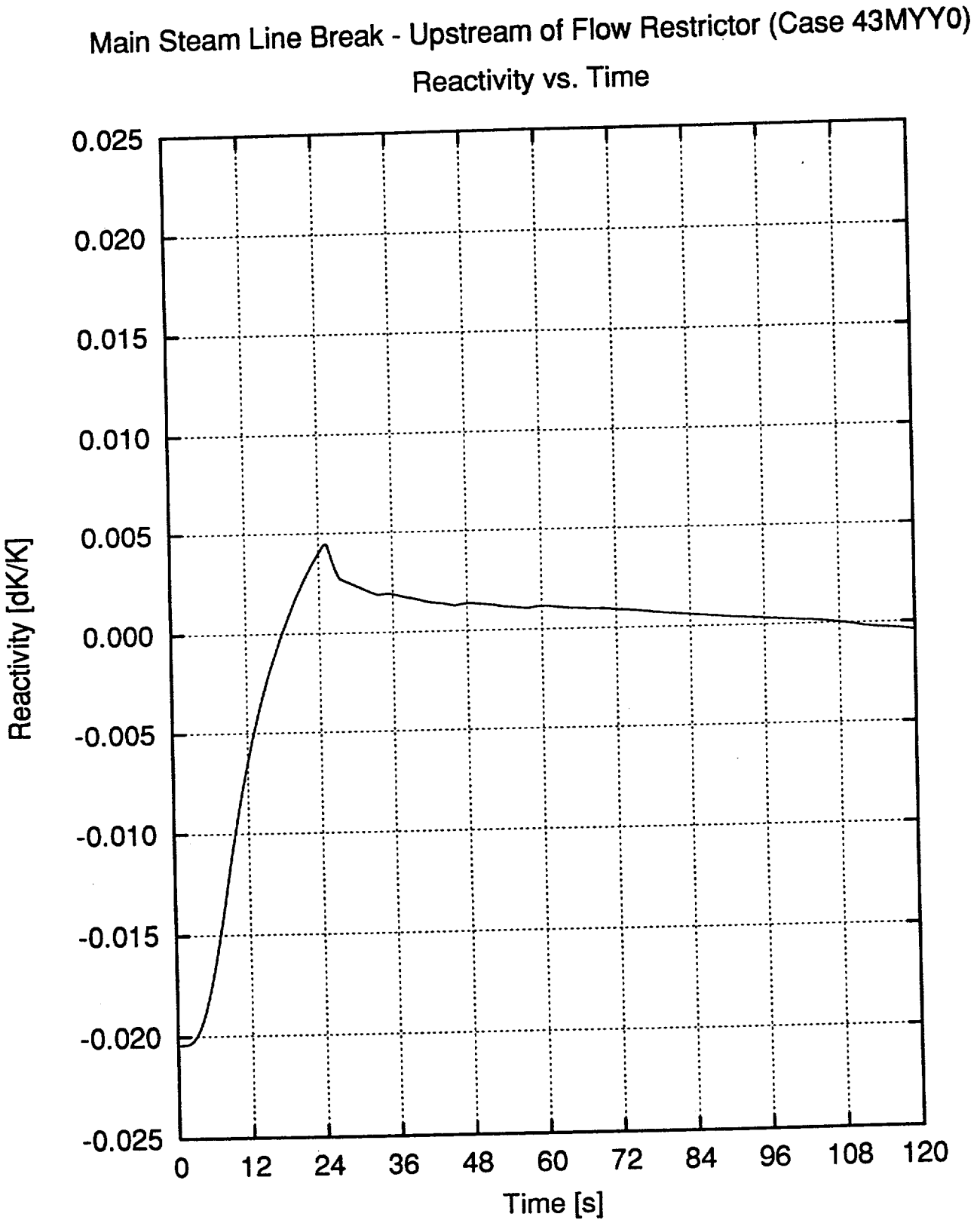

Figure 14.2.5-8

MAIN STEAM LINE BREAK

DOWNSTREAM FLOW RESTRICTOR

TAVE vs. TIME

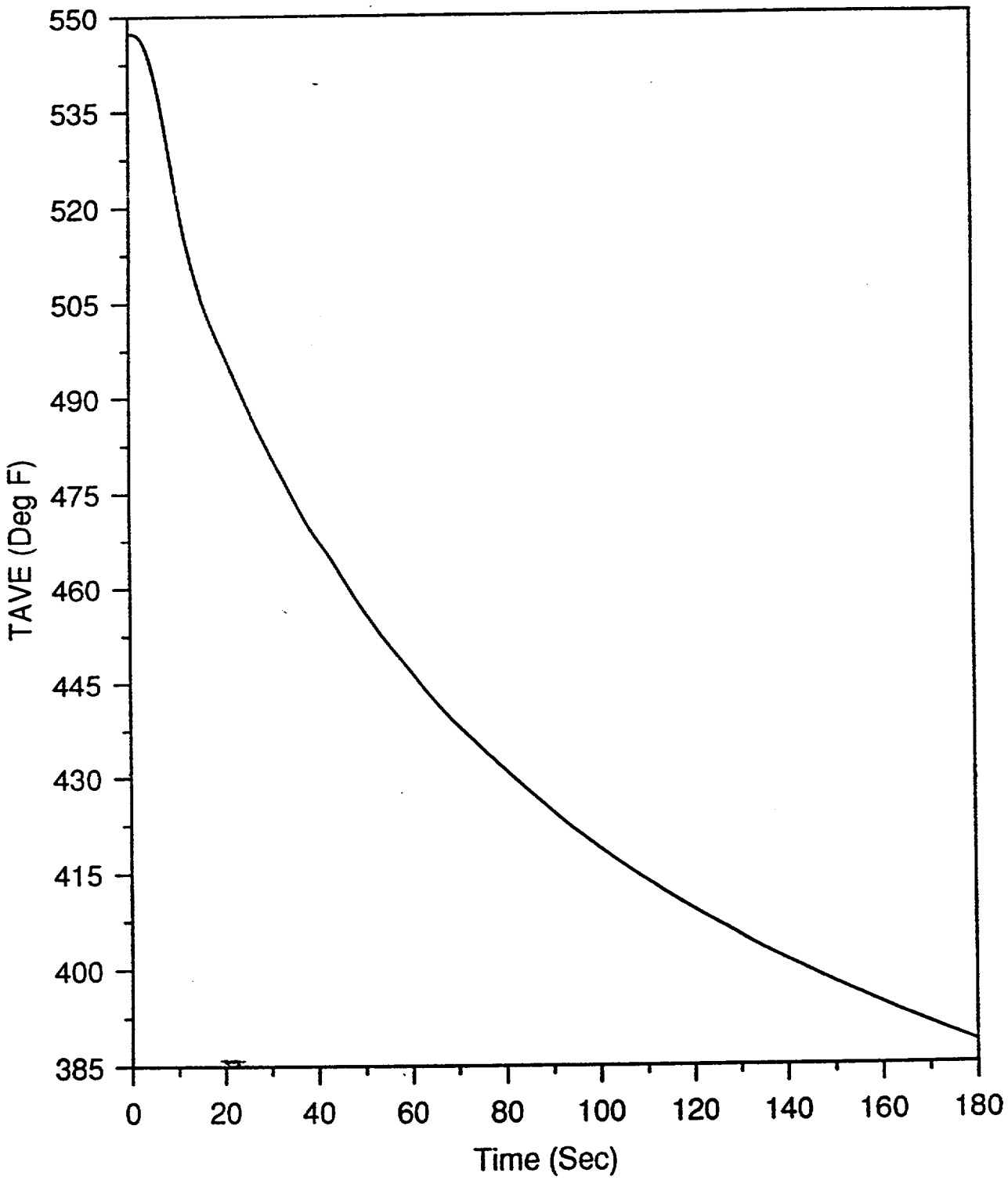

FIGURE 14.2.5-9

MAIN STEAM LINE BREAK

DOWNSTREAM FLOW RESTRICTOR

PRESSURIZER PRESSURE vs. TIME

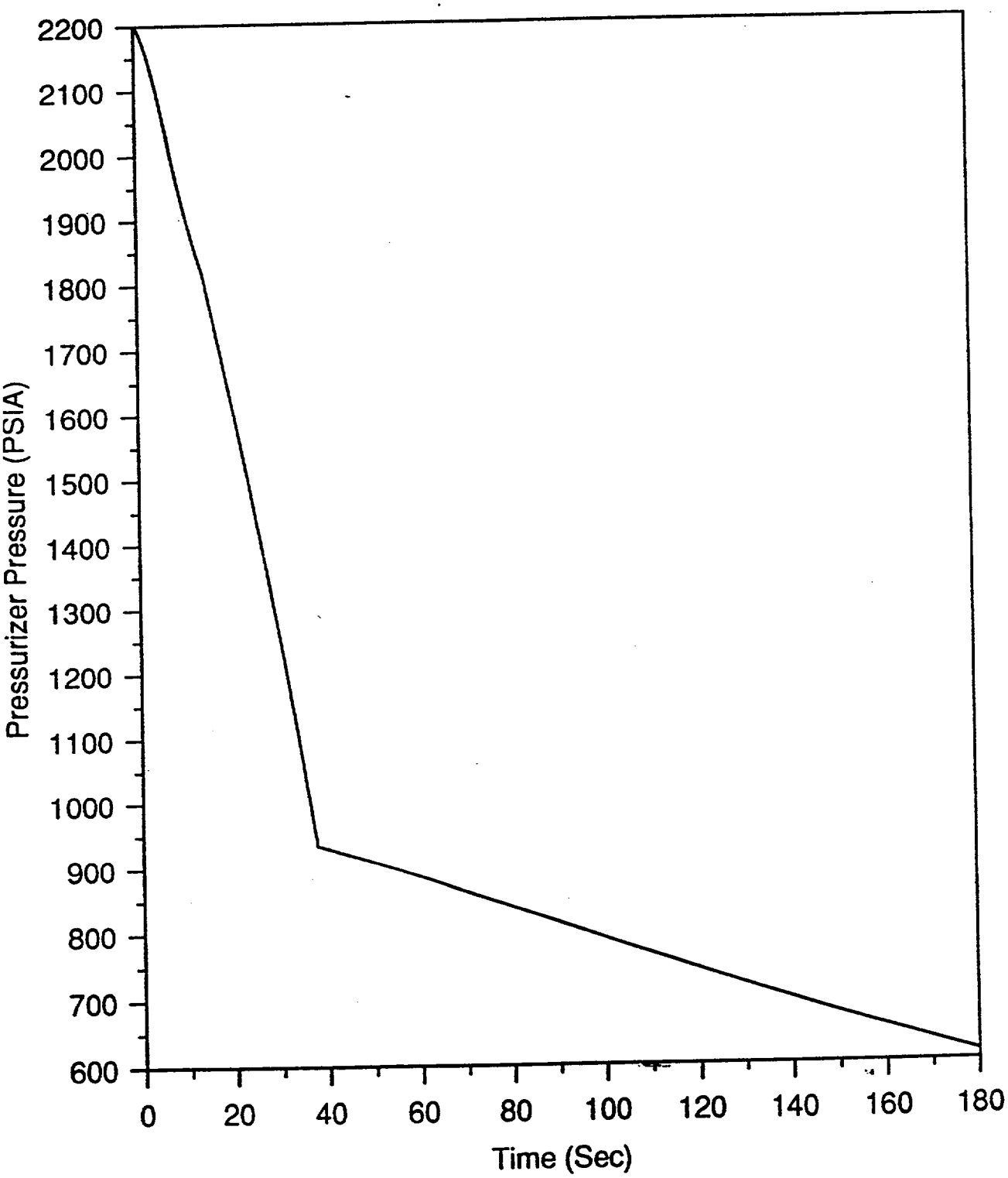

FIGURE 14.2.5-10

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MAIN STEAM LINE BREAK

DOWNSTREAM FLOW RESTRICTOR

HEAT FLUX vs. TIME

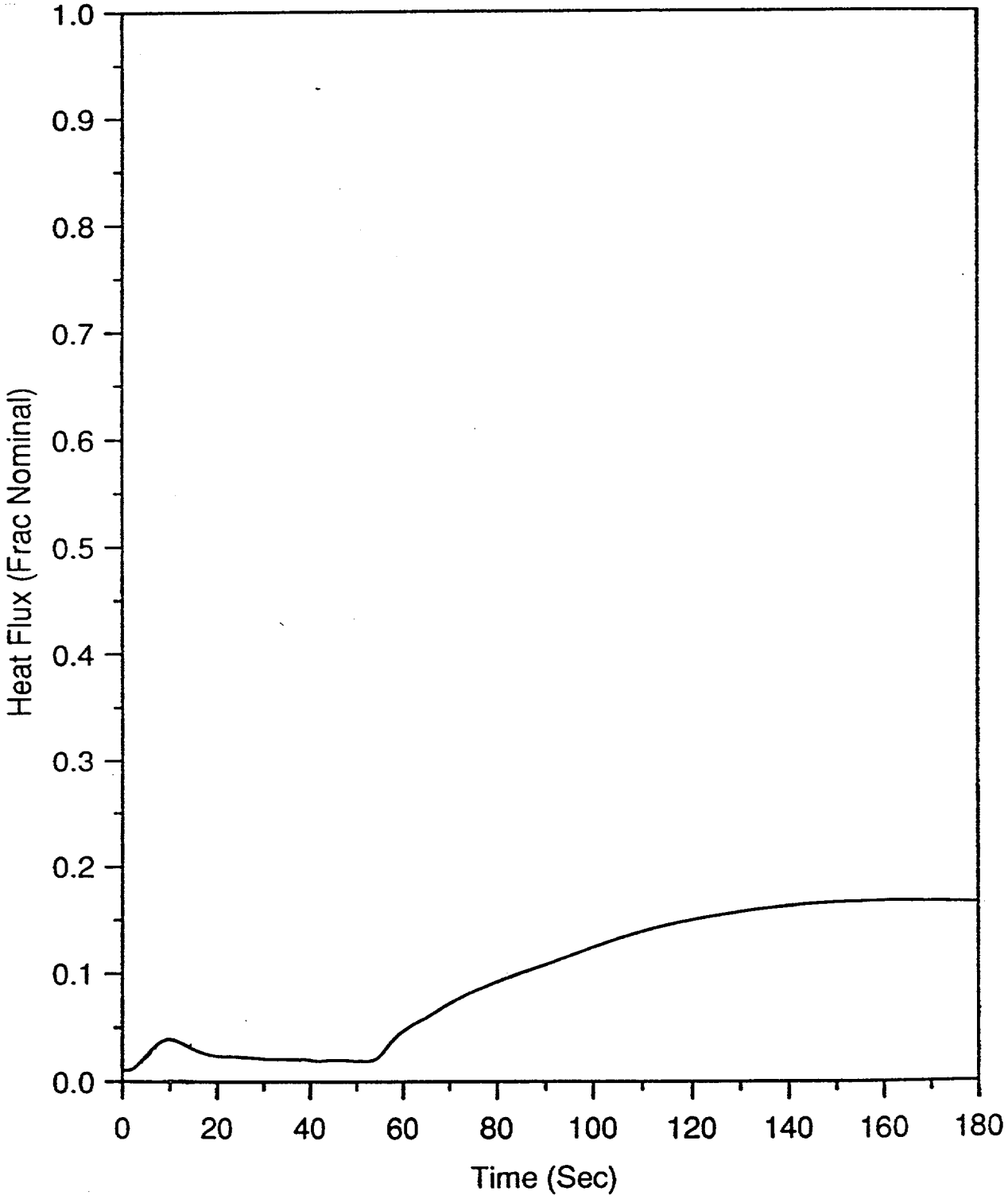

FIGURE 14.2.5-11

MAIN STEAM LINE BREAK

DOWNSTREAM FLOW RESTRICTOR

SG2 BREAK FLOW vs. TIME

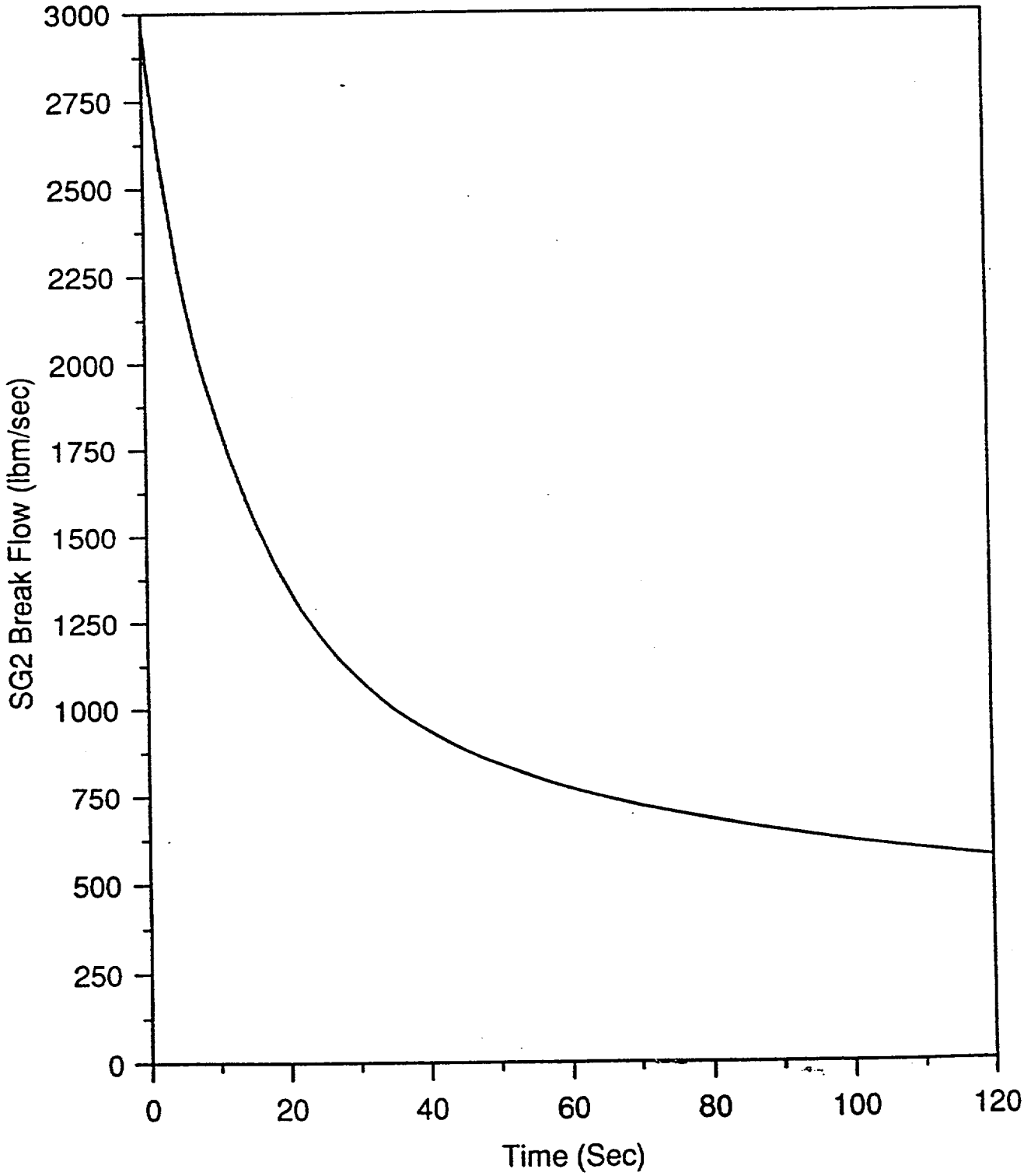

FIGURE 14.2.5-12

Rev. 15
05/01/99

MAIN STEAM LINE BREAK

DOWNSTREAM FLOW RESTRICTOR

REACTIVITY vs. TIME

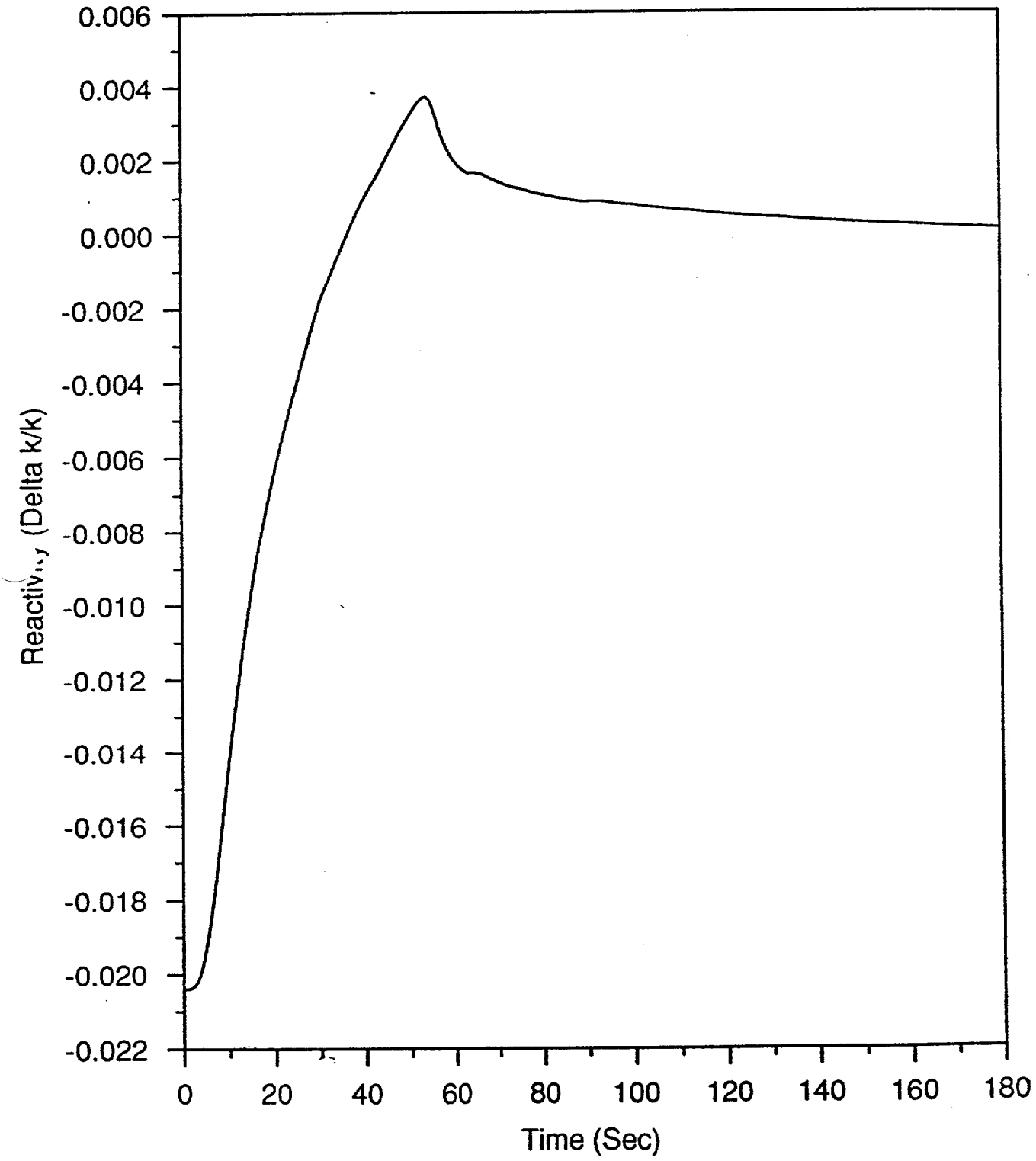

FIGURE 14.2.5-13

MAIN STEAM LINE BREAK

SPURIOUS OPENING OF SAFETY VALVE

TAVE vs. TIME

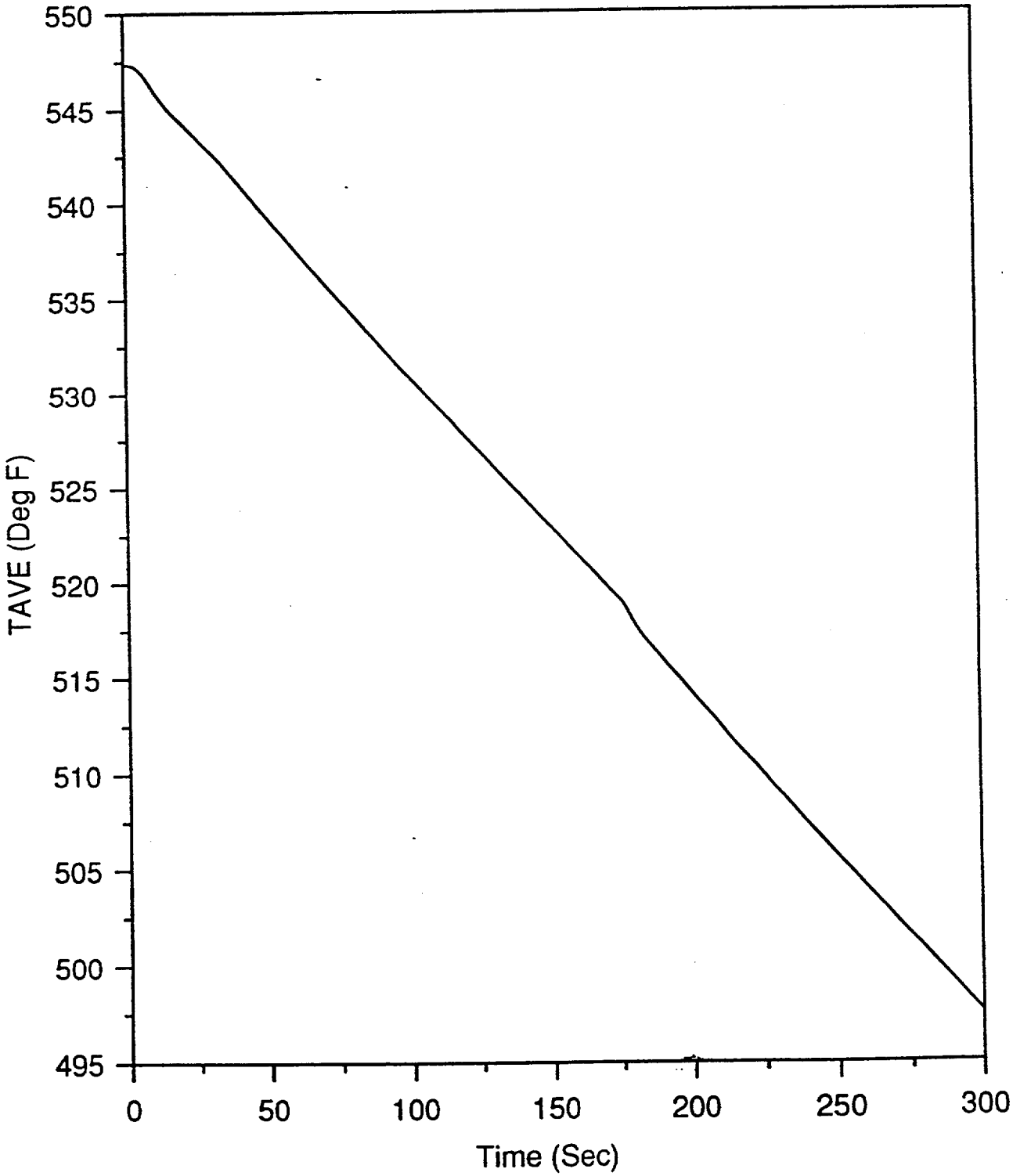

FIGURE 14.2.5-14

MAIN STEAM LINE BREAK

SPURIOUS OPENING OF SAFETY VALVE

PRESSURIZER PRESSURE vs. TIME

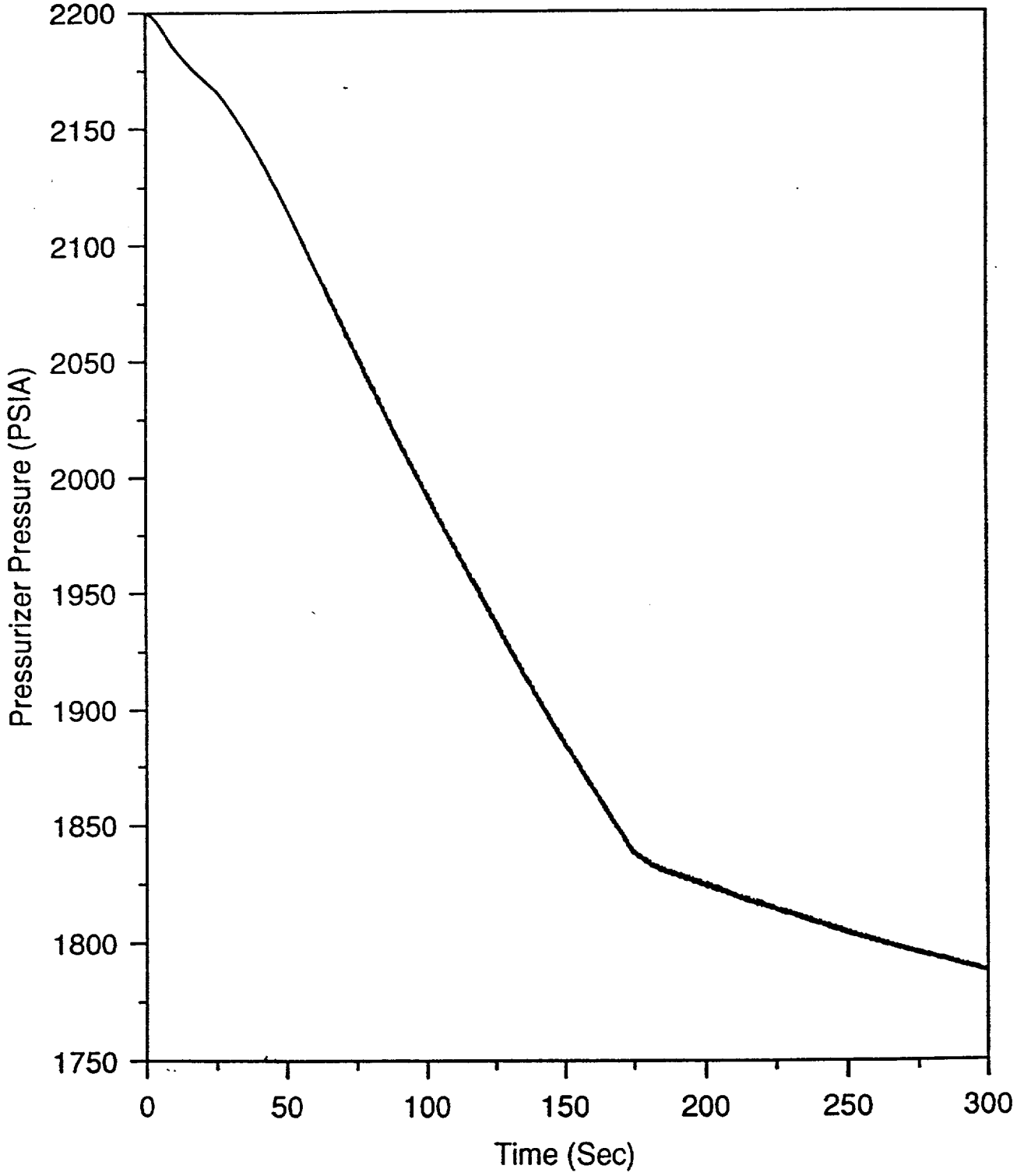

FIGURE 14.2.5-15

MAIN STEAM LINE BREAK

SPURIOUS OPENING OF SAFETY VALVE

HEAT FLUX vs. TIME

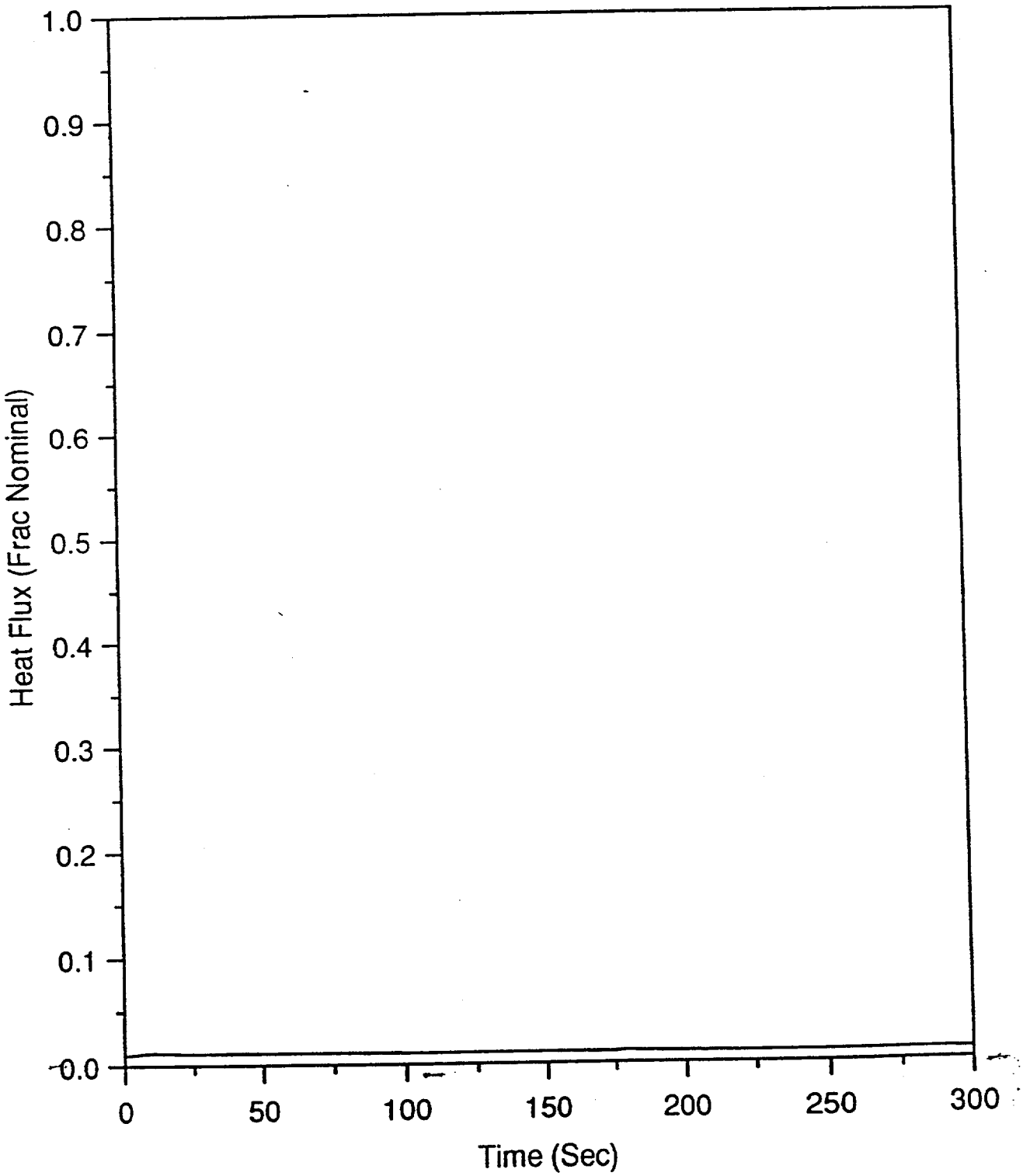

FIGURE 14.2.5-16

Rev. 15
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MAIN STEAM LINE BREAK

SPURIOUS OPENING OF SAFETY VALVE

SG2 BREAK FLOW vs. TIME

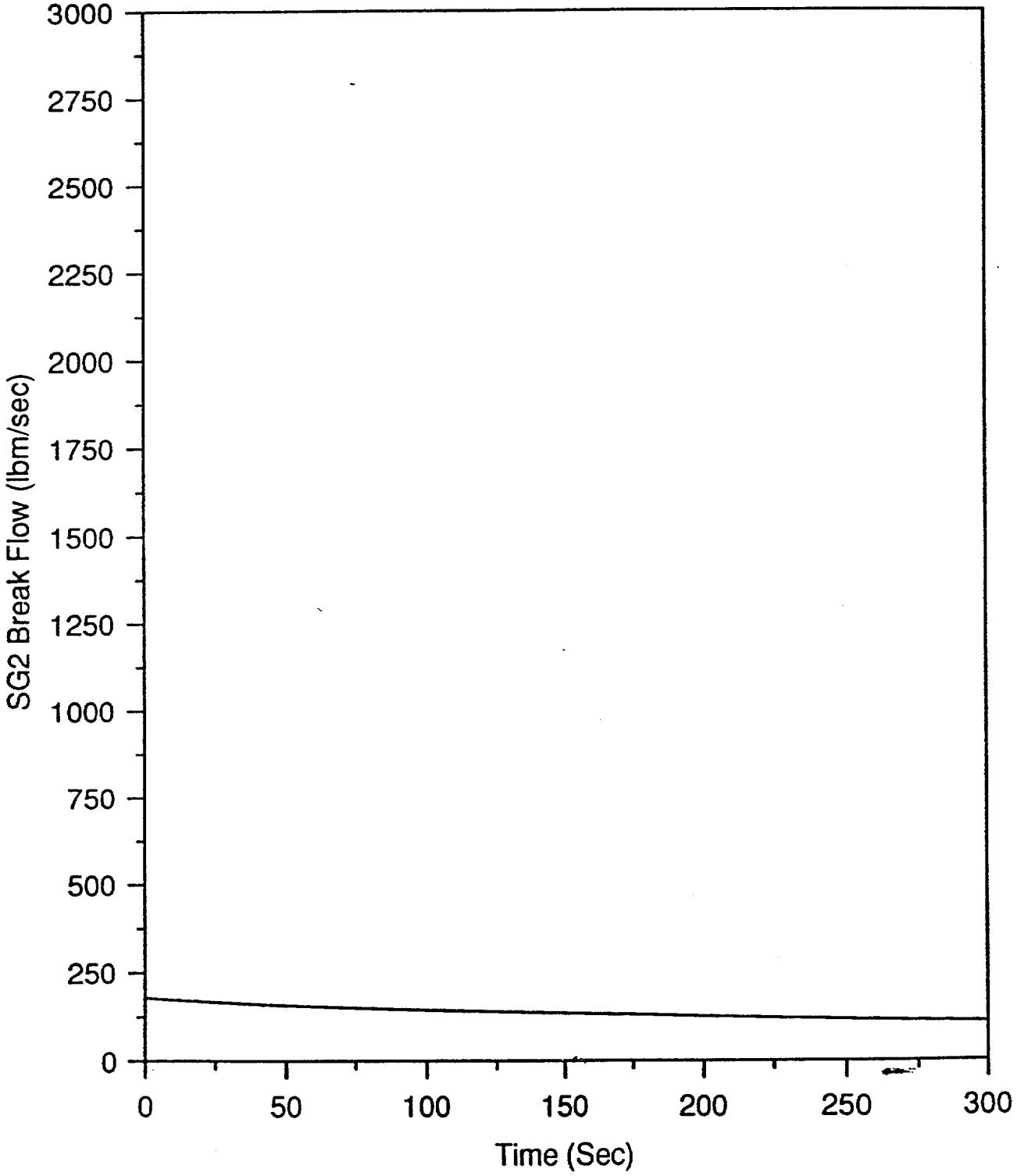

FIGURE 14.2.5-17

Rev. 15
05/01/99

MAIN STEAM LINE BREAK

SPURIOUS OPENING OF SAFETY VALVE

REACTIVITY vs. TIME

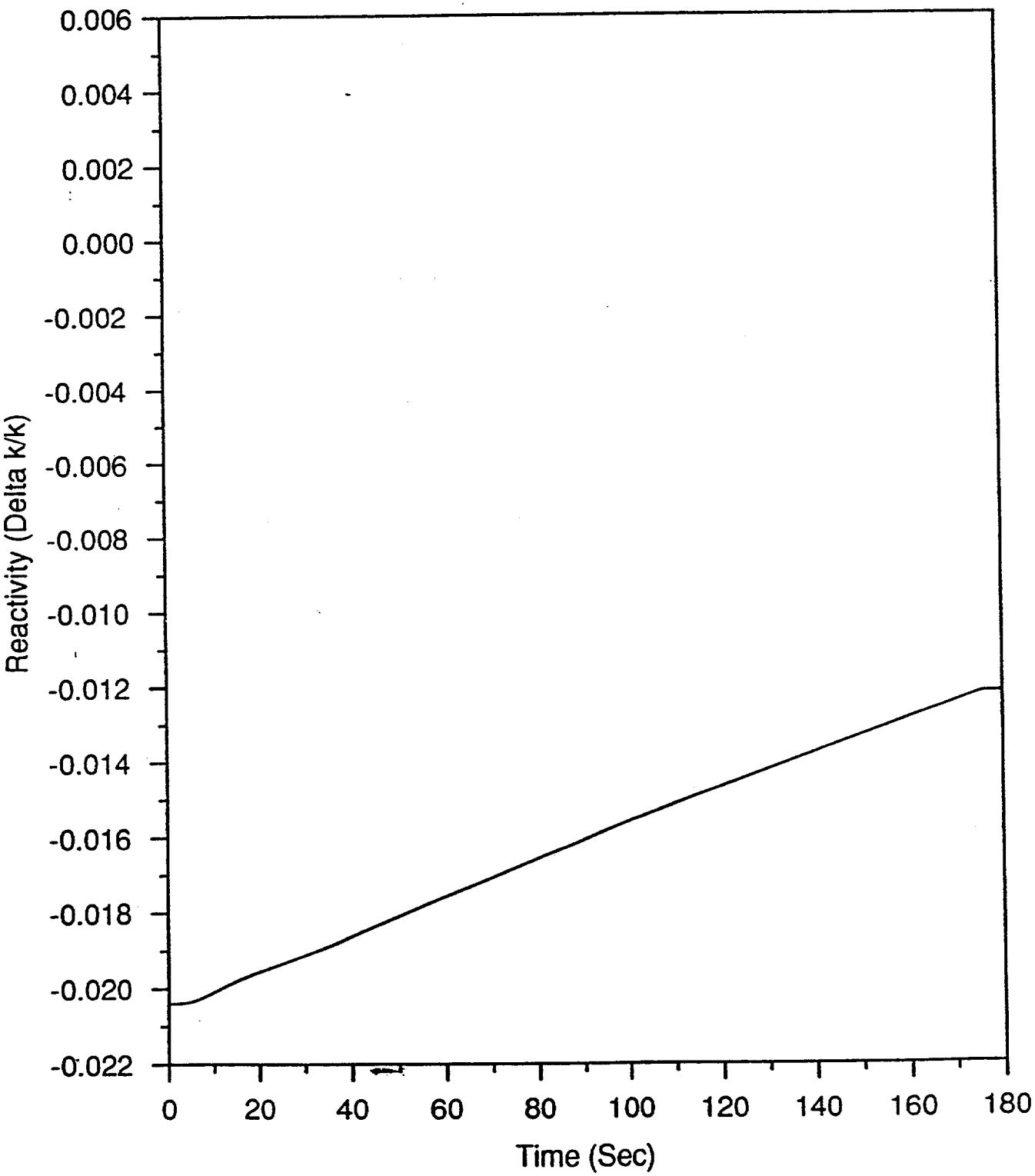

FIGURE 14.2.5-18

MAIN STEAM LINE BREAK

CONTAINMENT PRESSURE RESPONSE

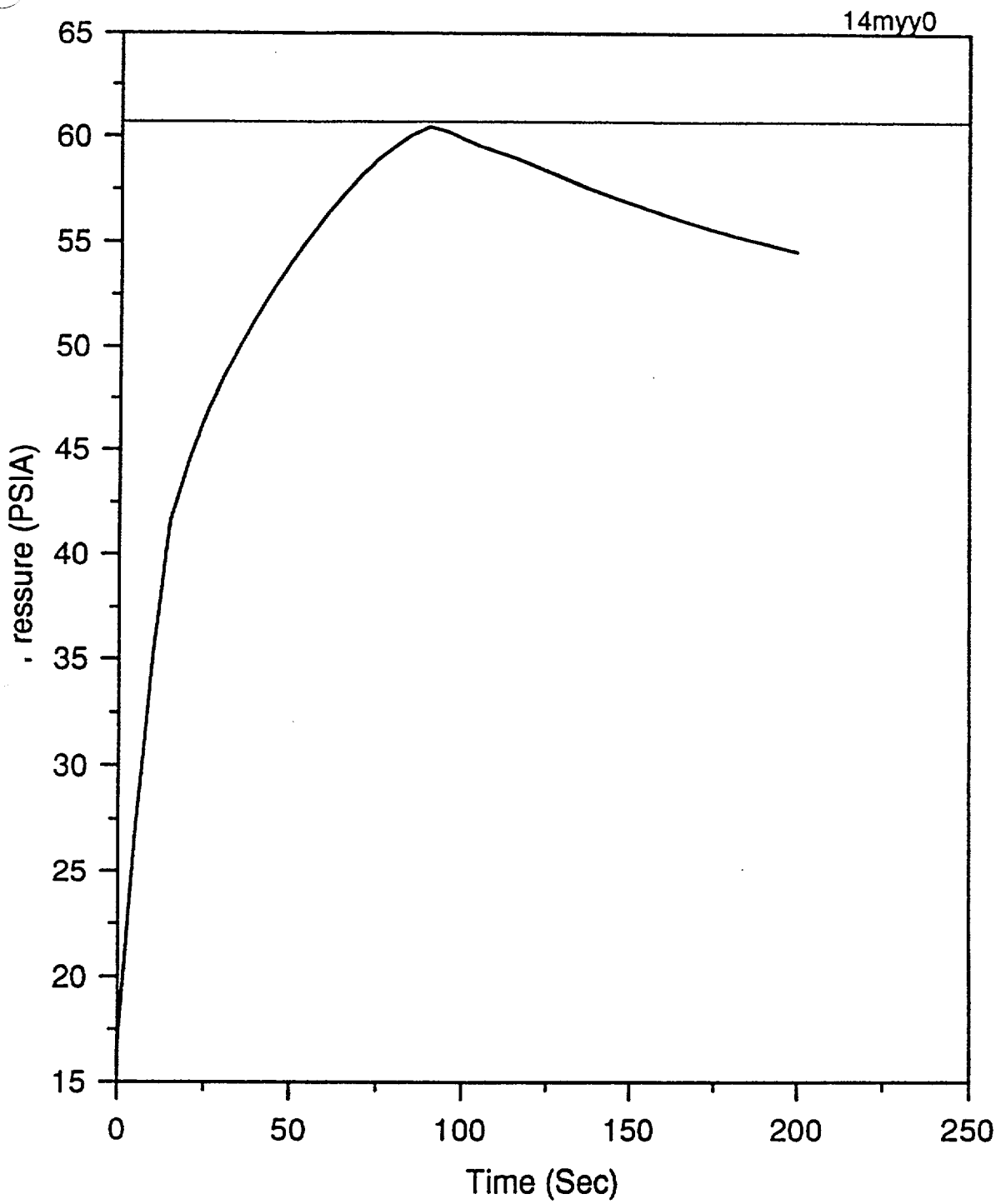

FIGURE 14.2.5-19

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MAIN STEAM LINE BREAK

CONTAINMENT TEMPERATURE RESPONSE

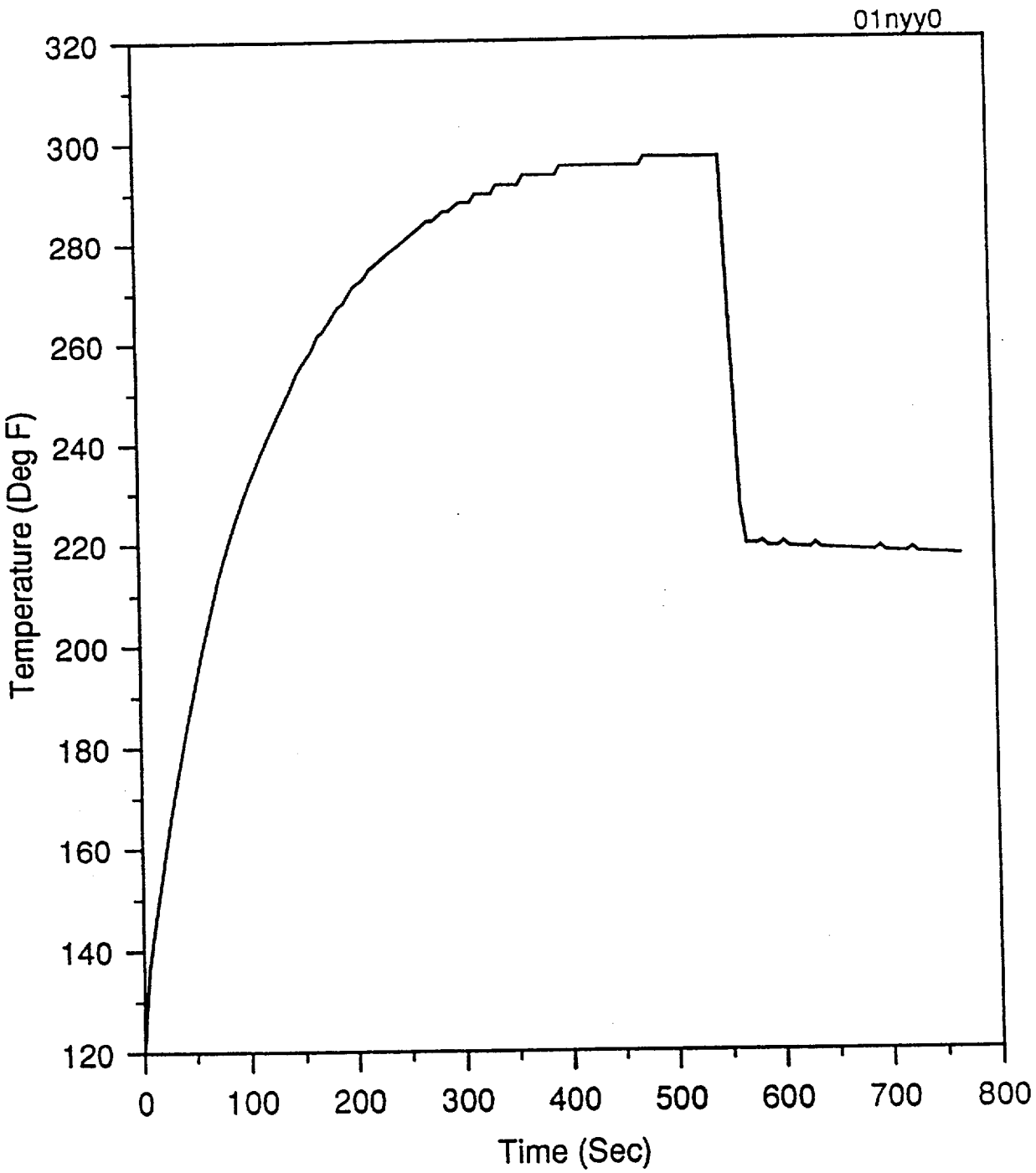

FIGURE 14.2.5-20

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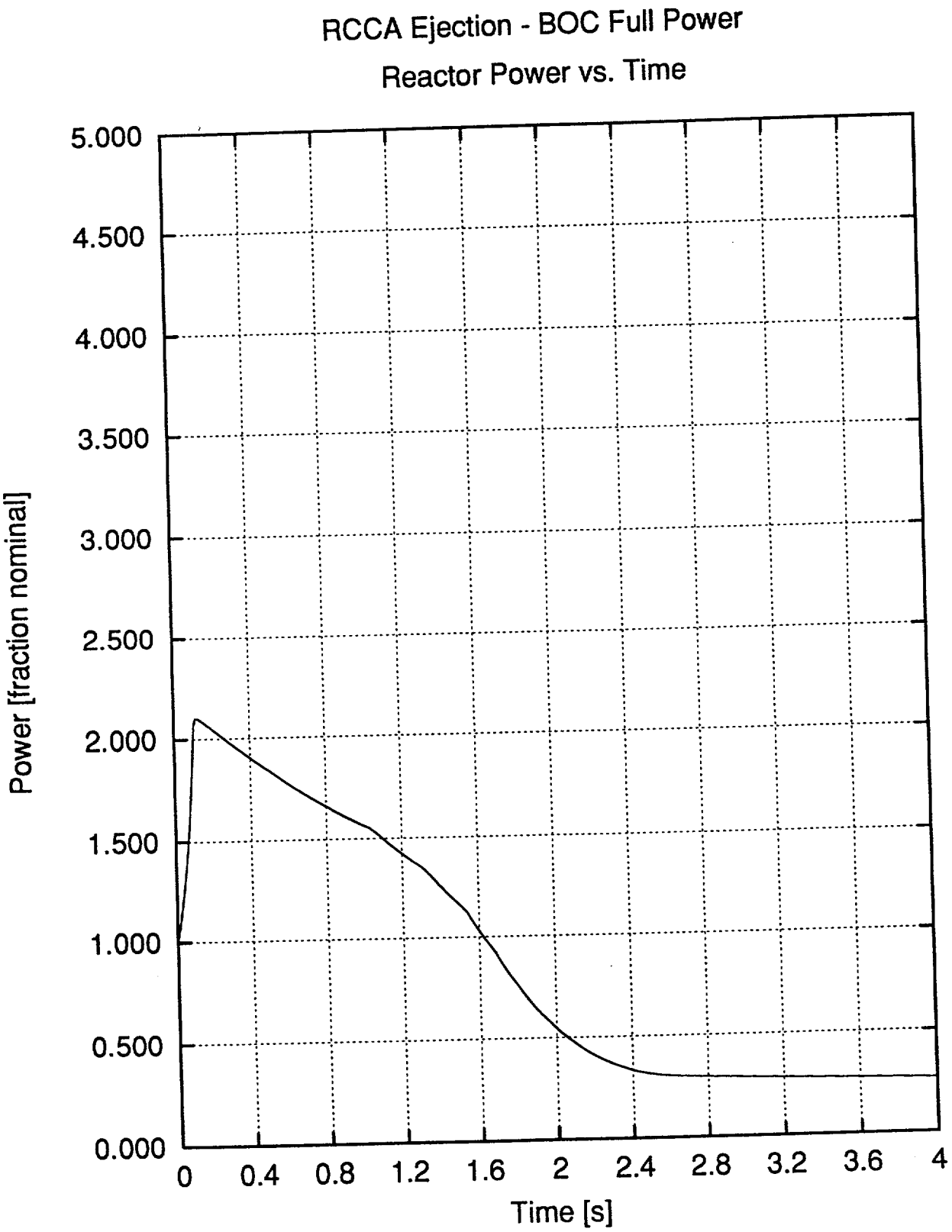

Figure 14.2.6-1

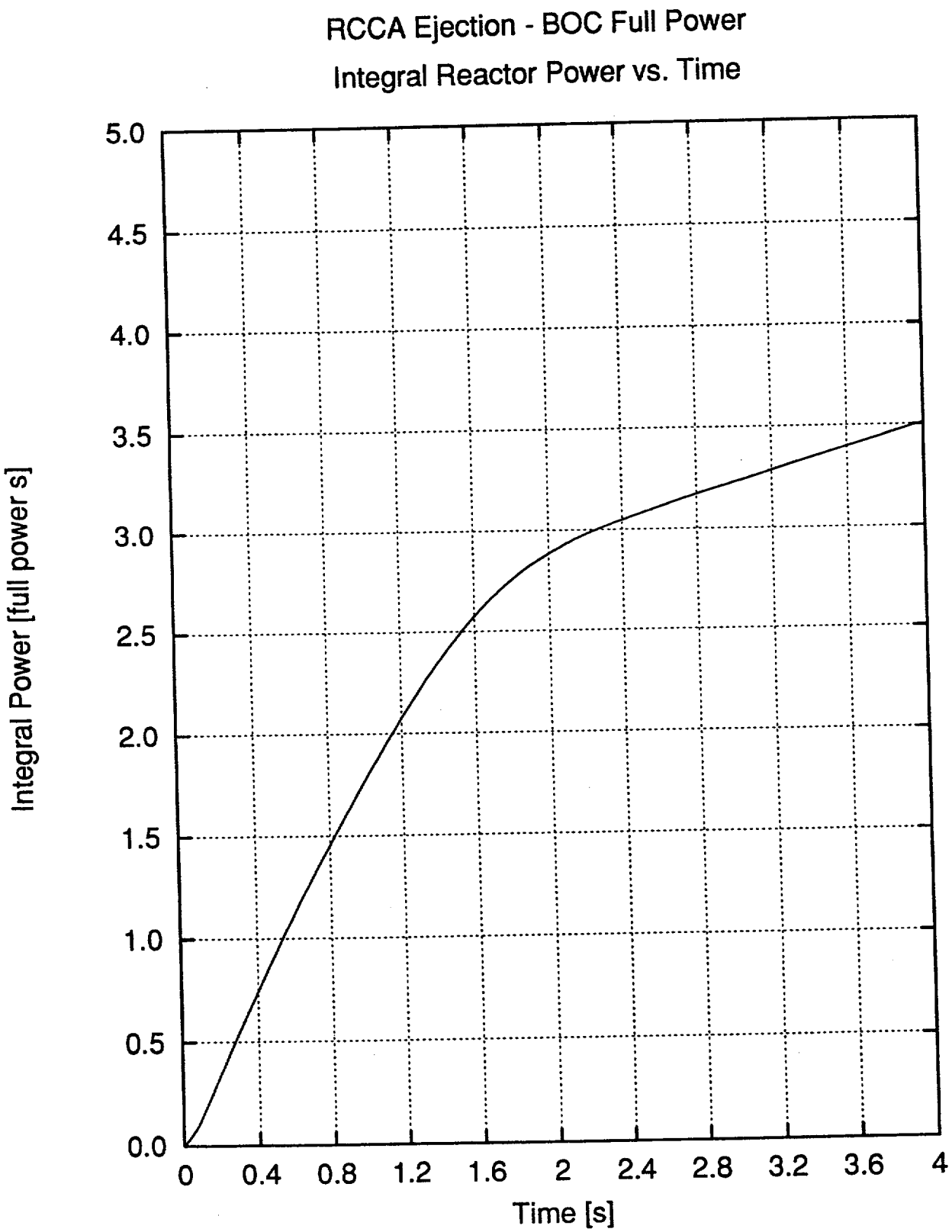

Figure 14.2.6-2

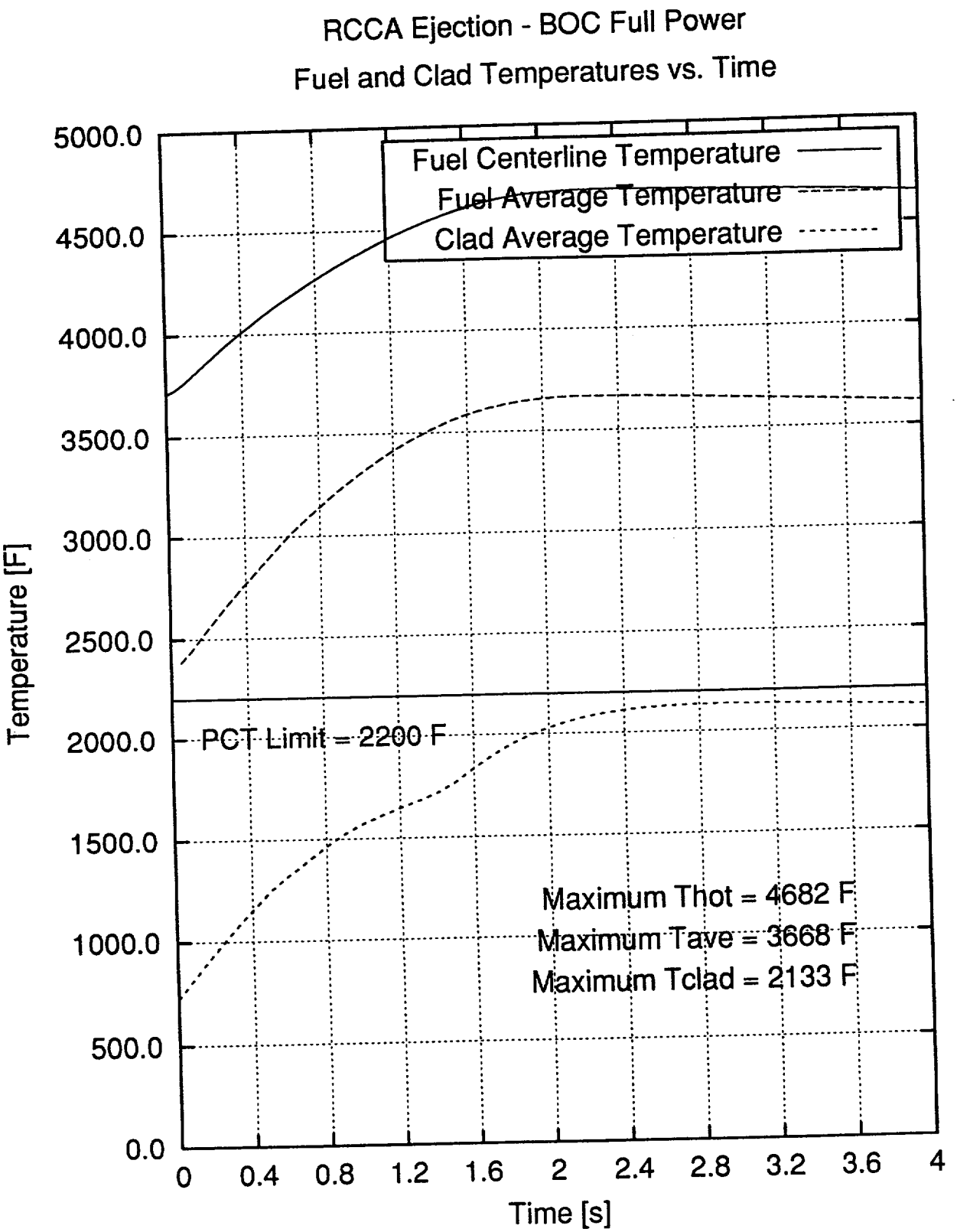

Figure 14.2.6-3

RCCA EJECTION

BOC ZERO POWER

REACTOR POWER vs. TIME

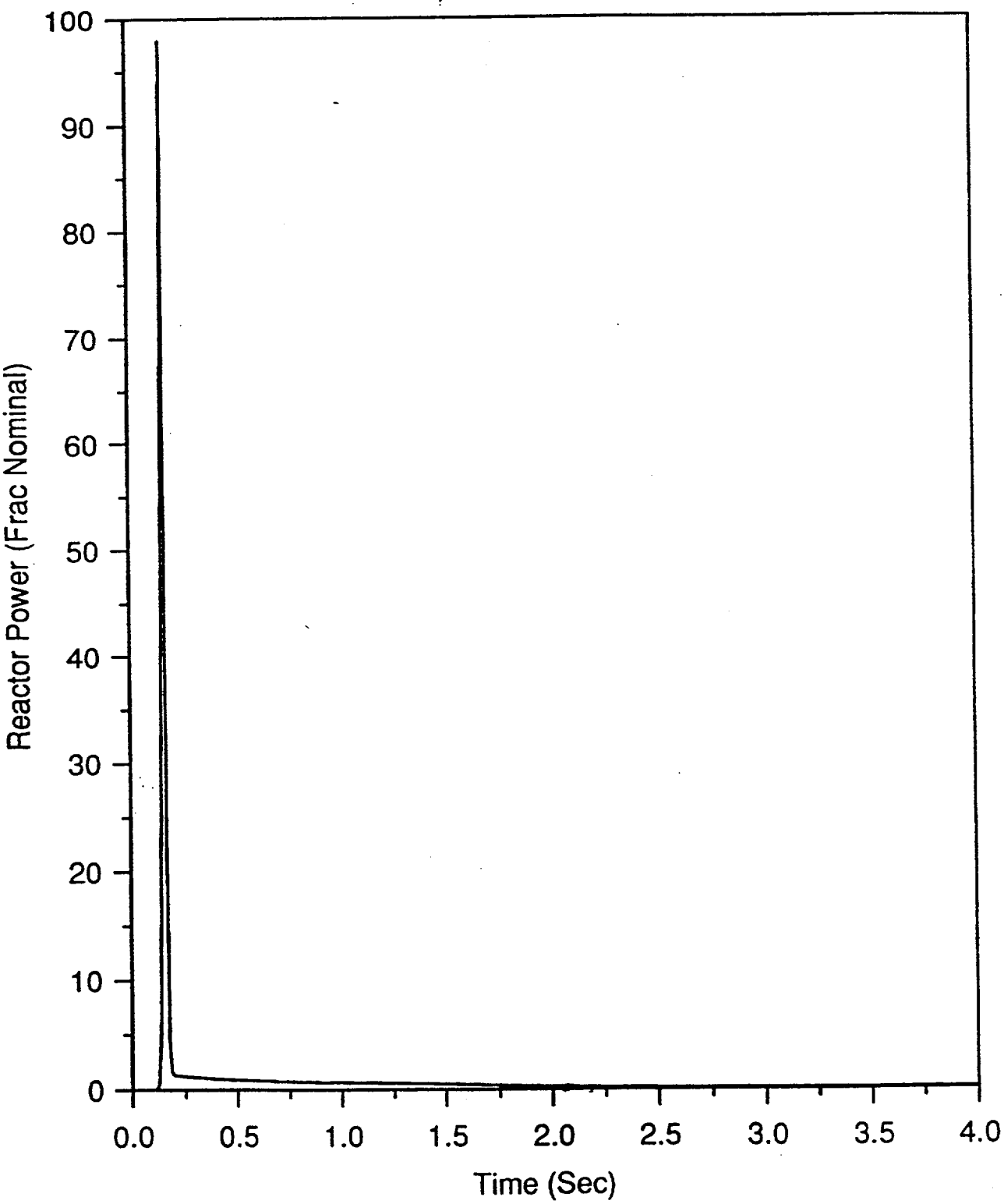

FIGURE 14.2.6-4

RCCA EJECTION

BOC ZERO POWER

REACTOR INTEGRATED POWER vs. TIME

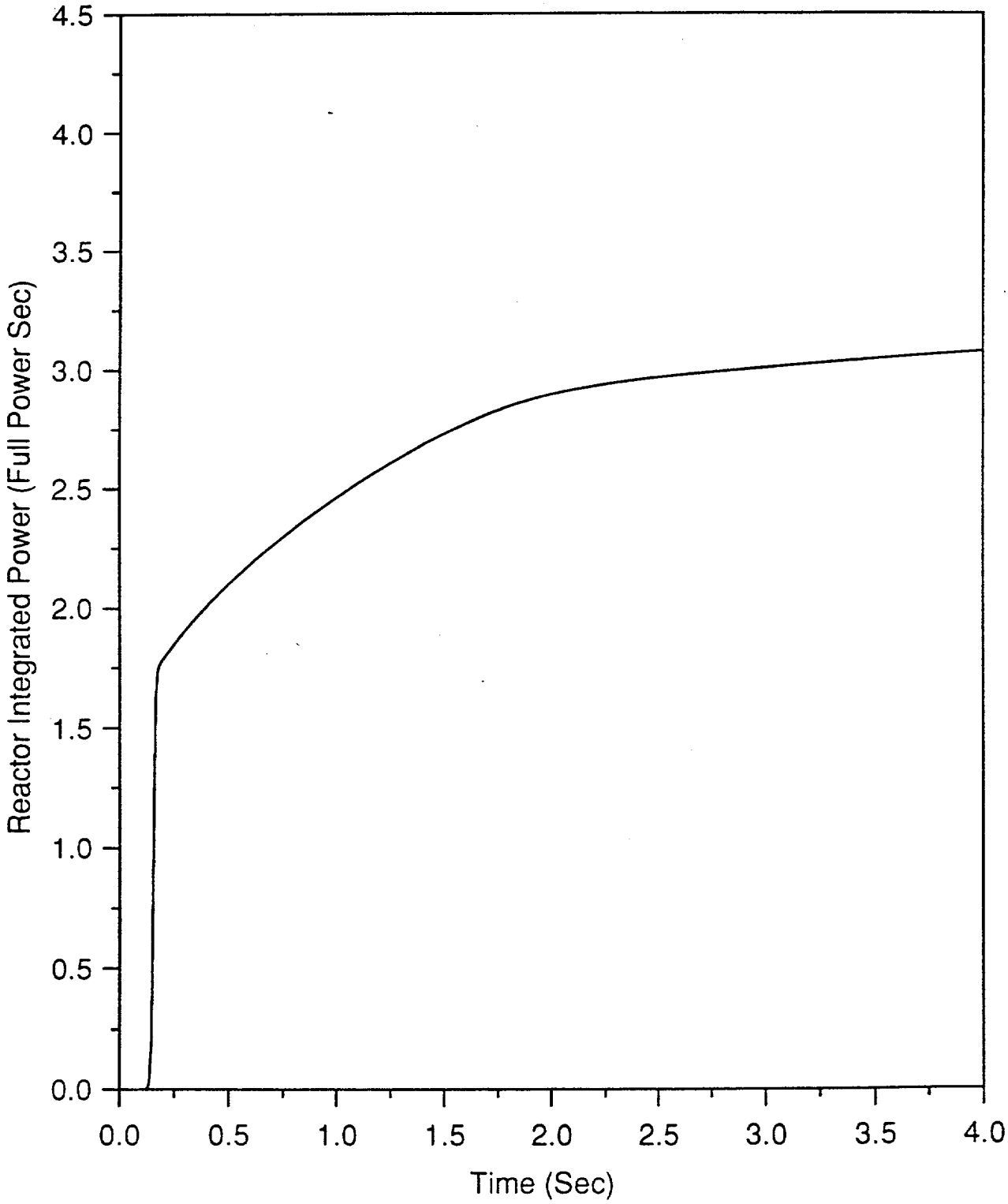

FIGURE 14.2.6-5

RCCA EJECTION

BOC ZERO POWER

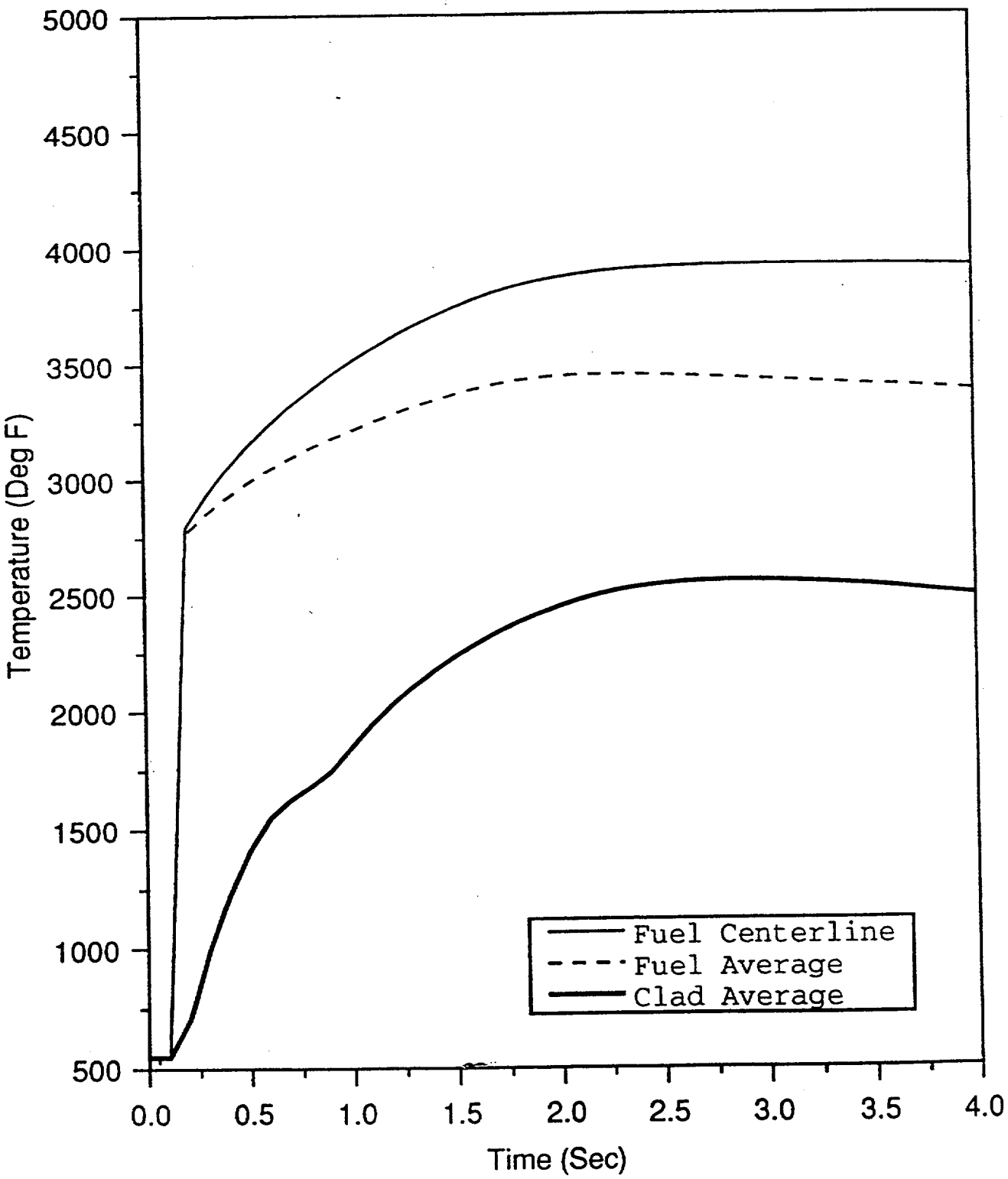

FIGURE 14.2.6-6

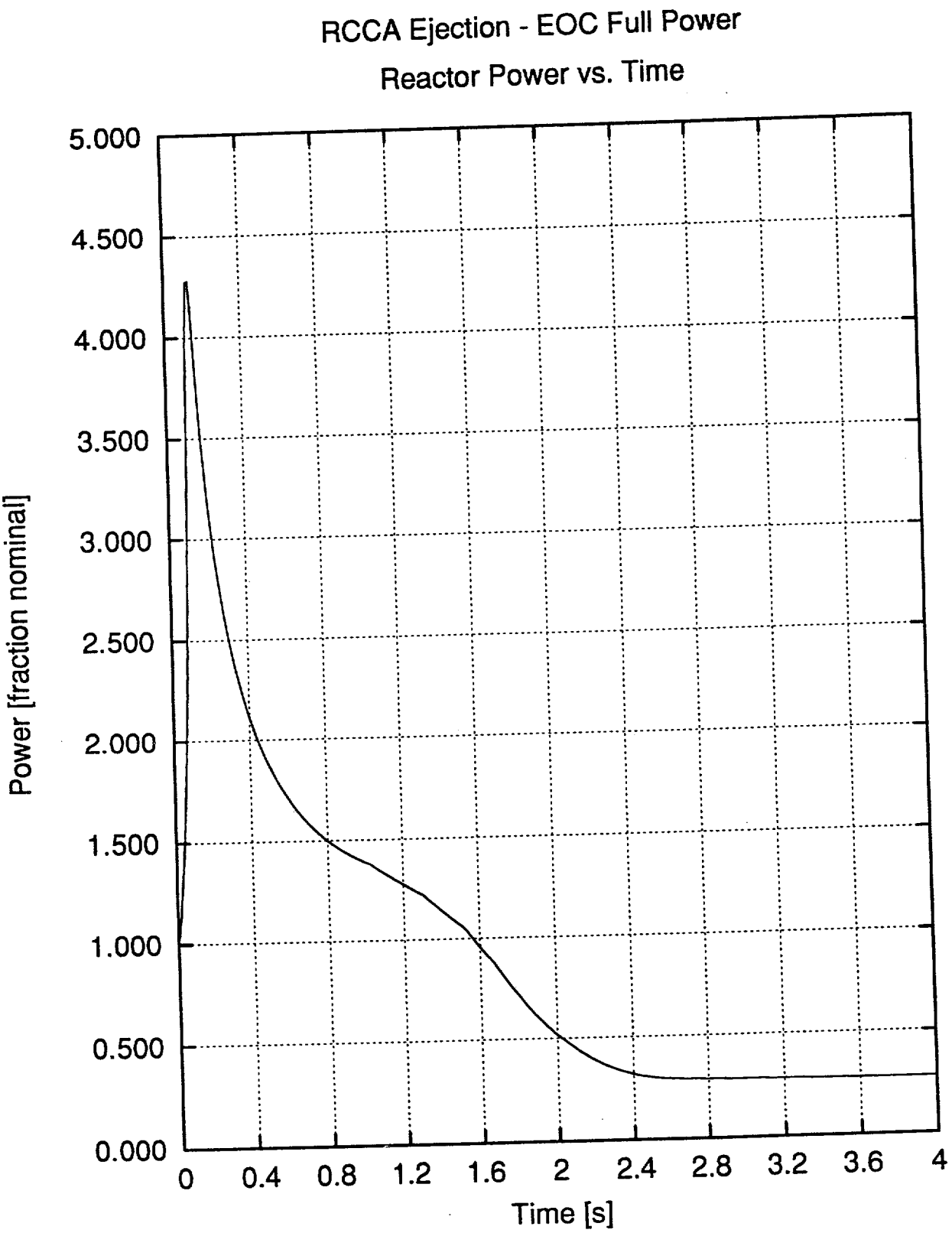

Figure 14.2.6-7

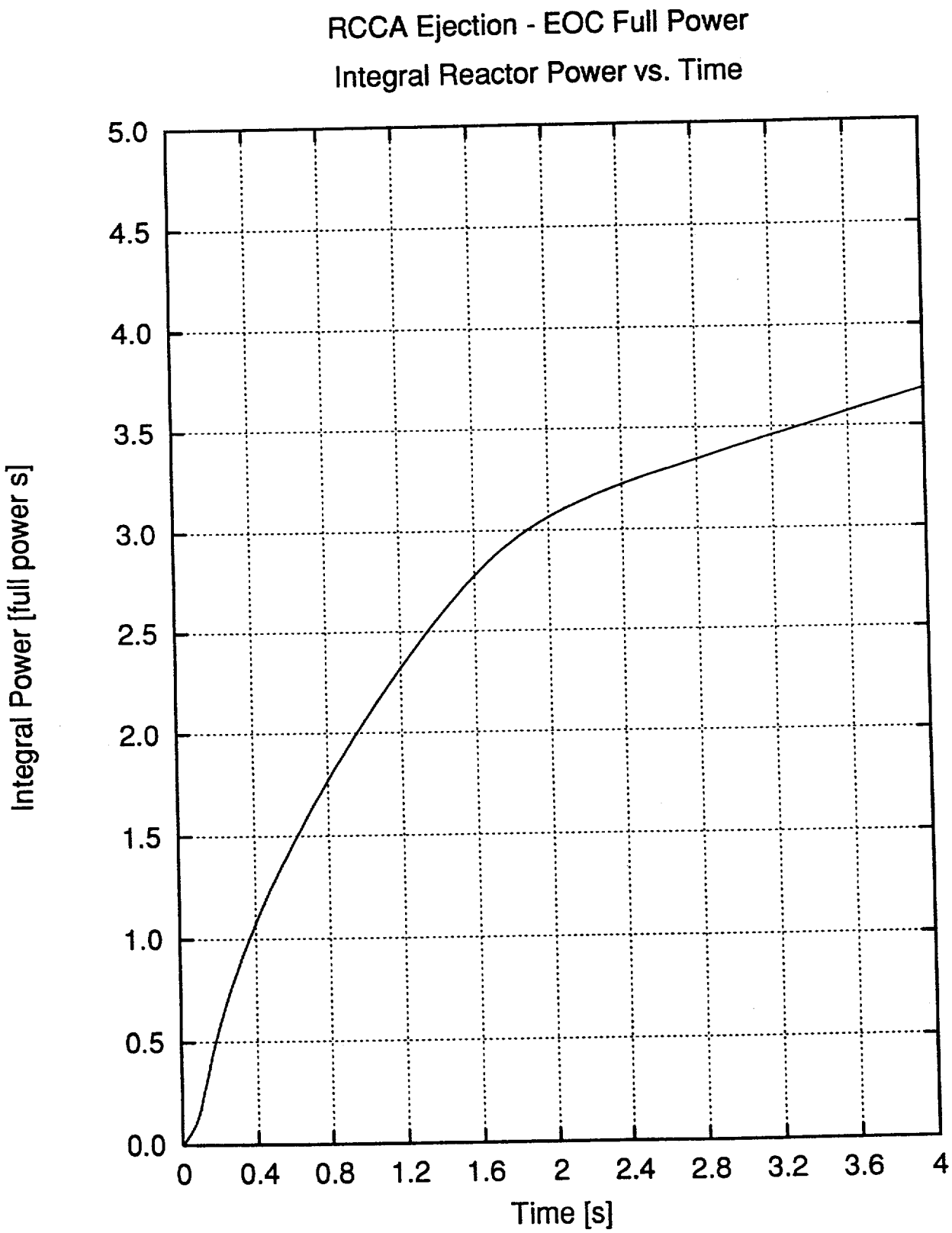

Figure 14.2.6-8

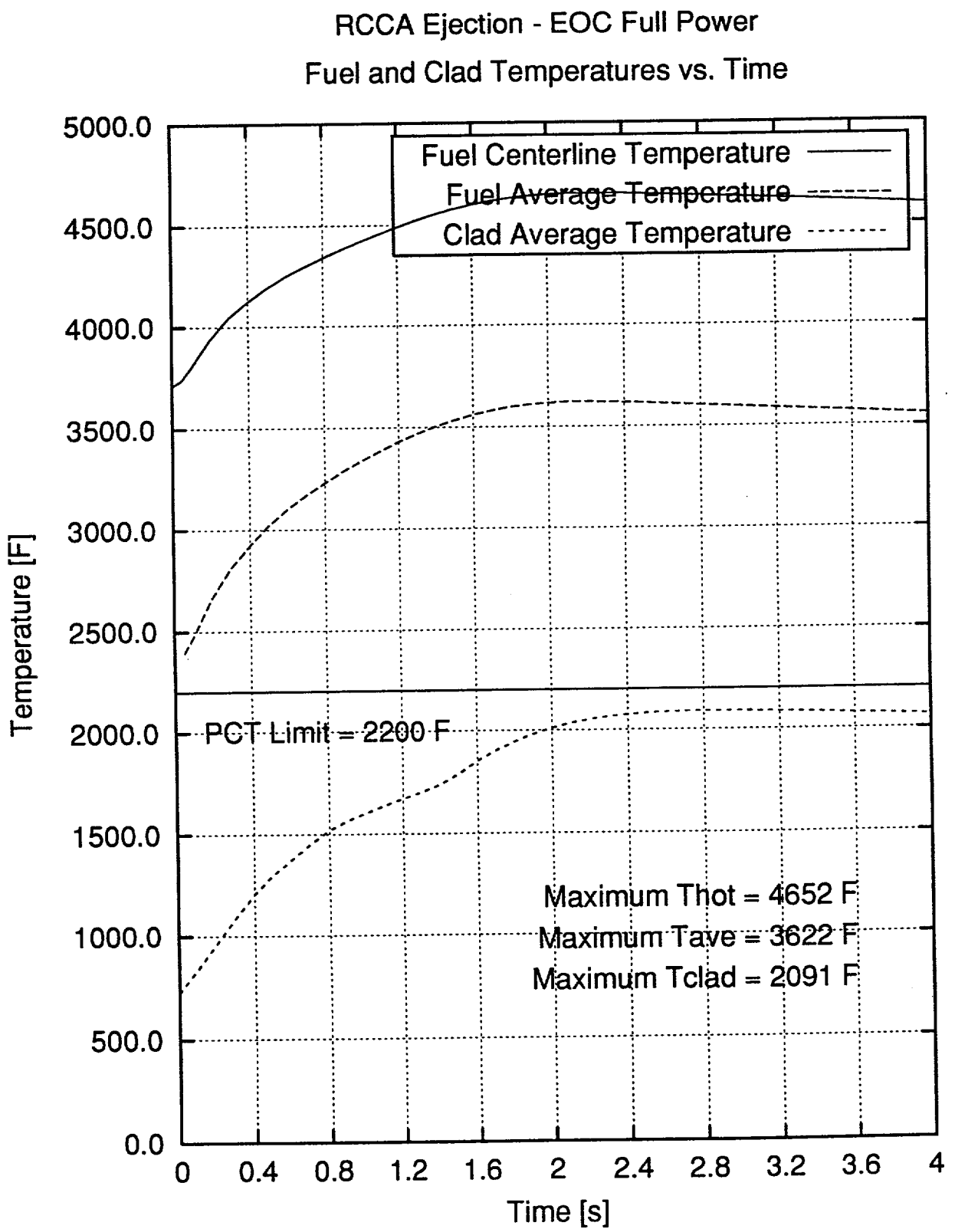

Figure 14.2.6-9

RCCA EJECTION

EOC ZERO POWER

REACTOR POWER vs. TIME

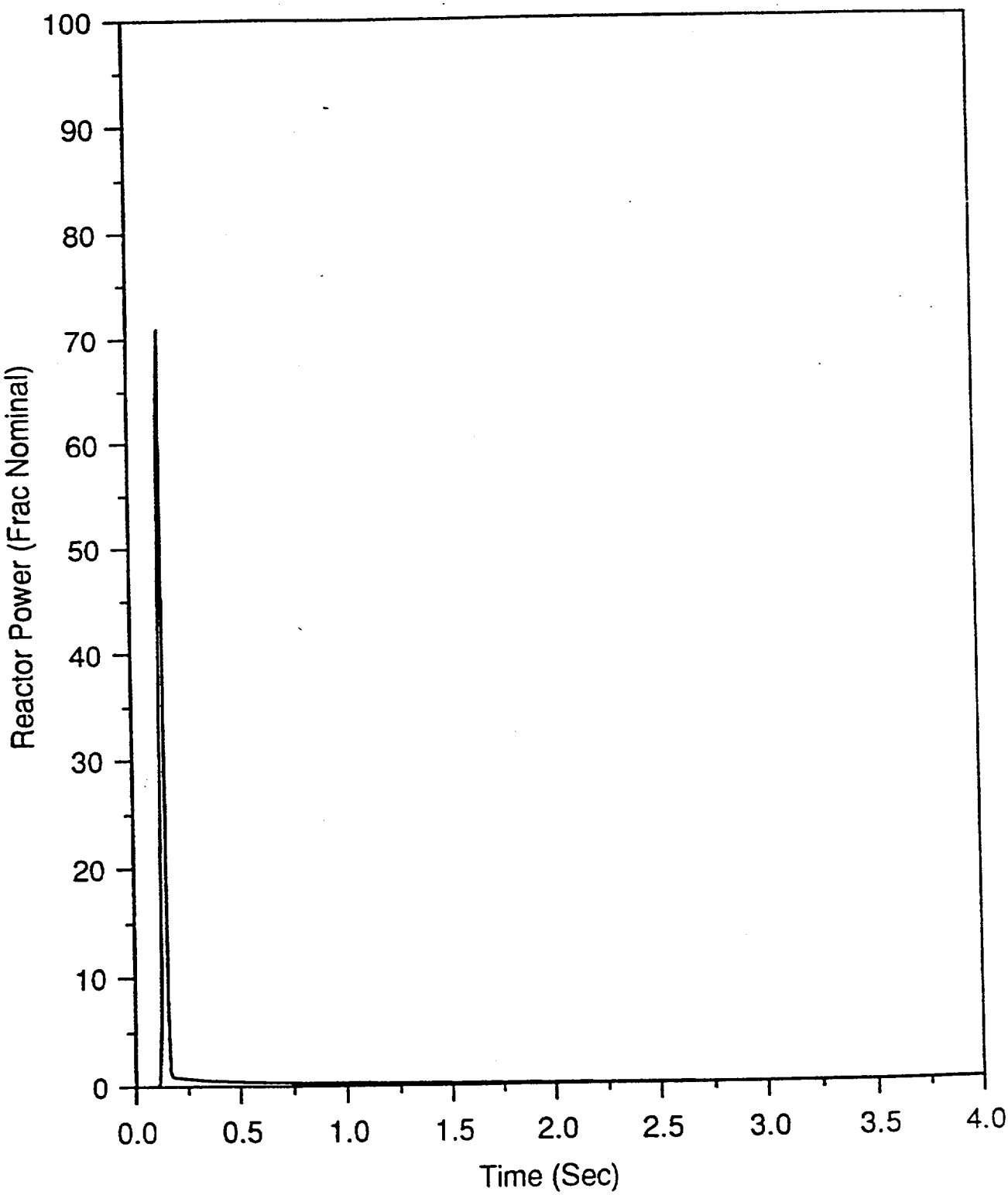

FIGURE 14.2.6-10

RCCA EJECTION

EOC ZERO POWER

REACTOR INTEGRATED POWER vs. TIME

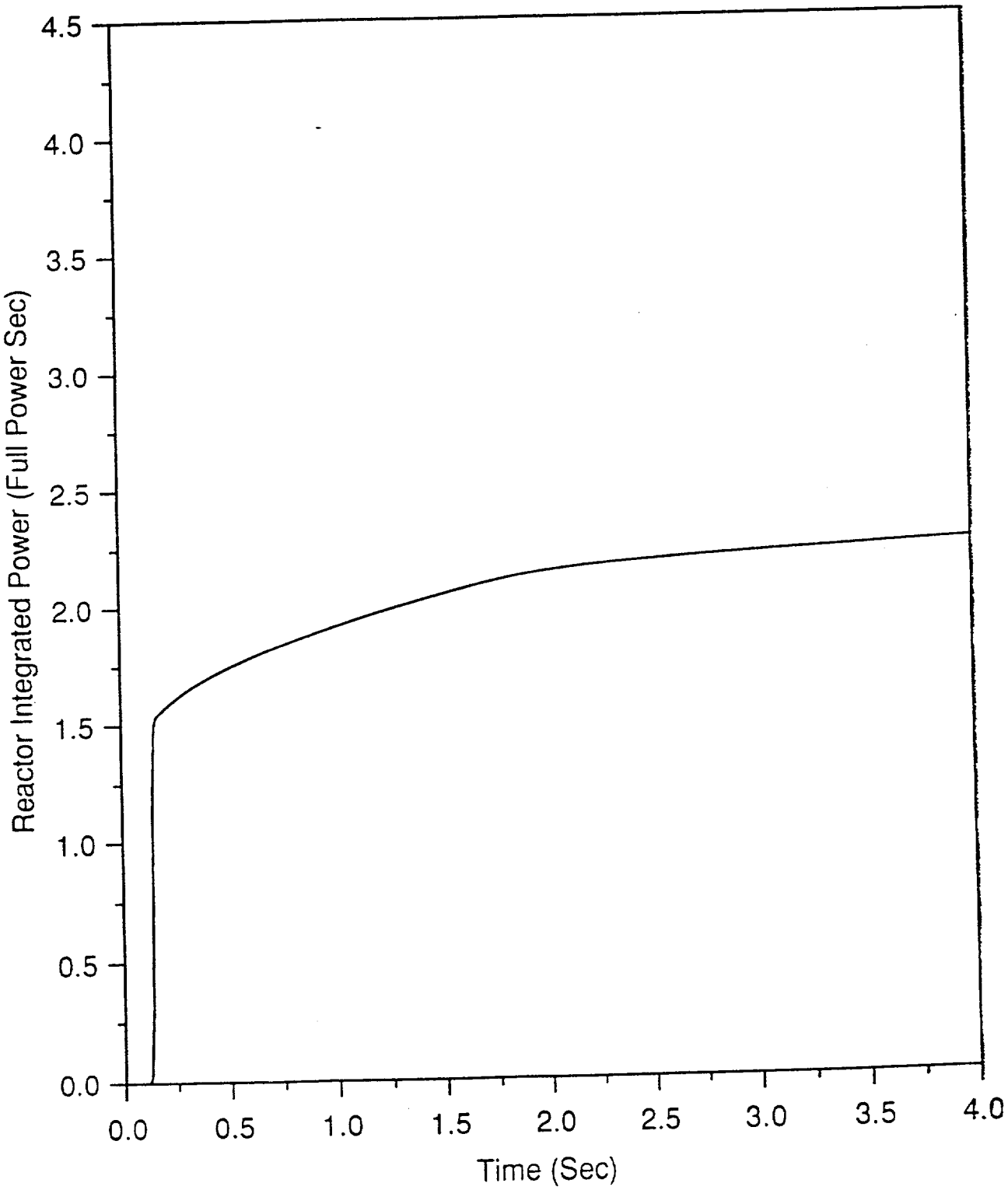

FIGURE 14.2.6-11

RCCA EJECTION

EOC ZERO POWER

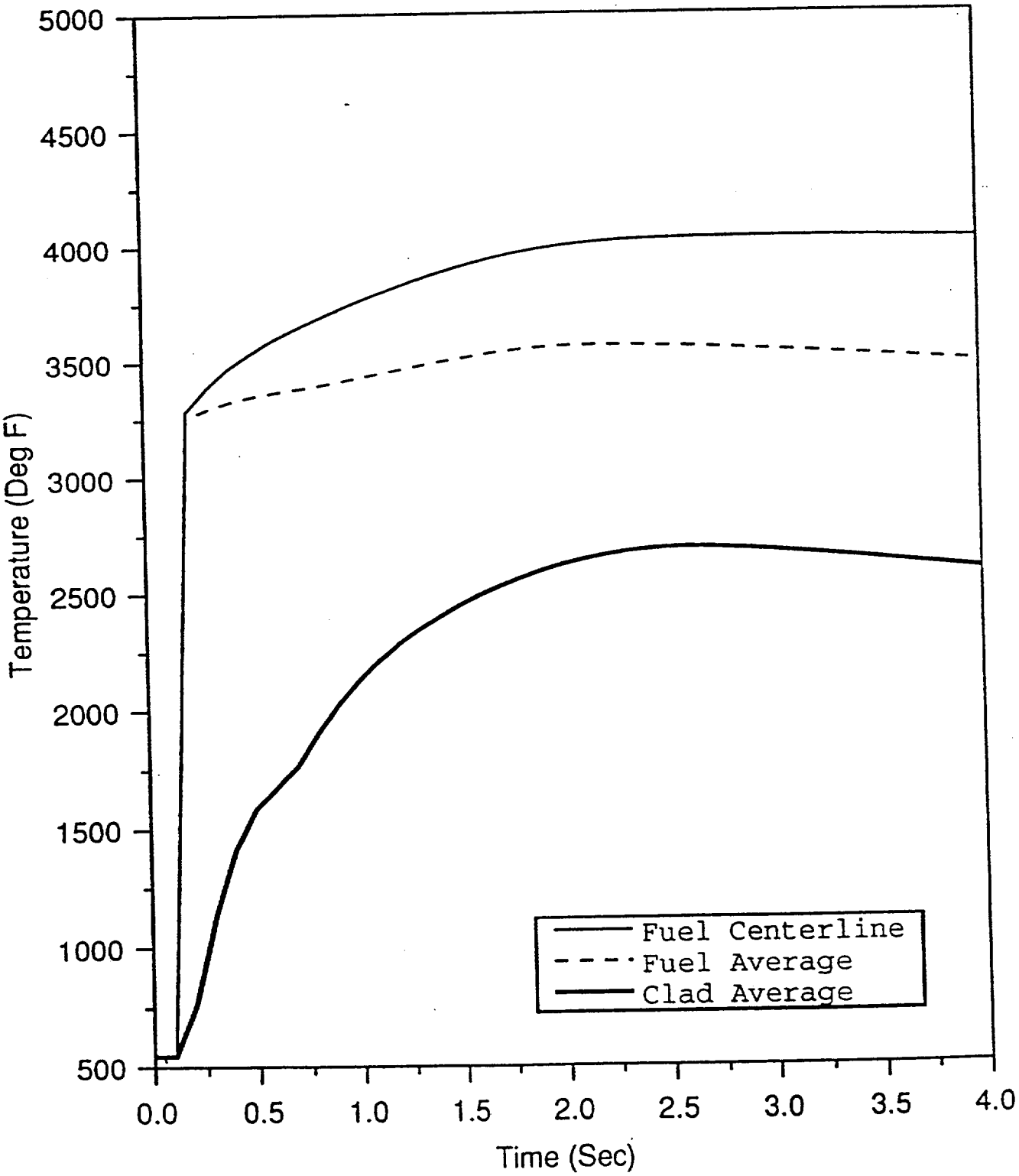

FIGURE 14.2.6-12

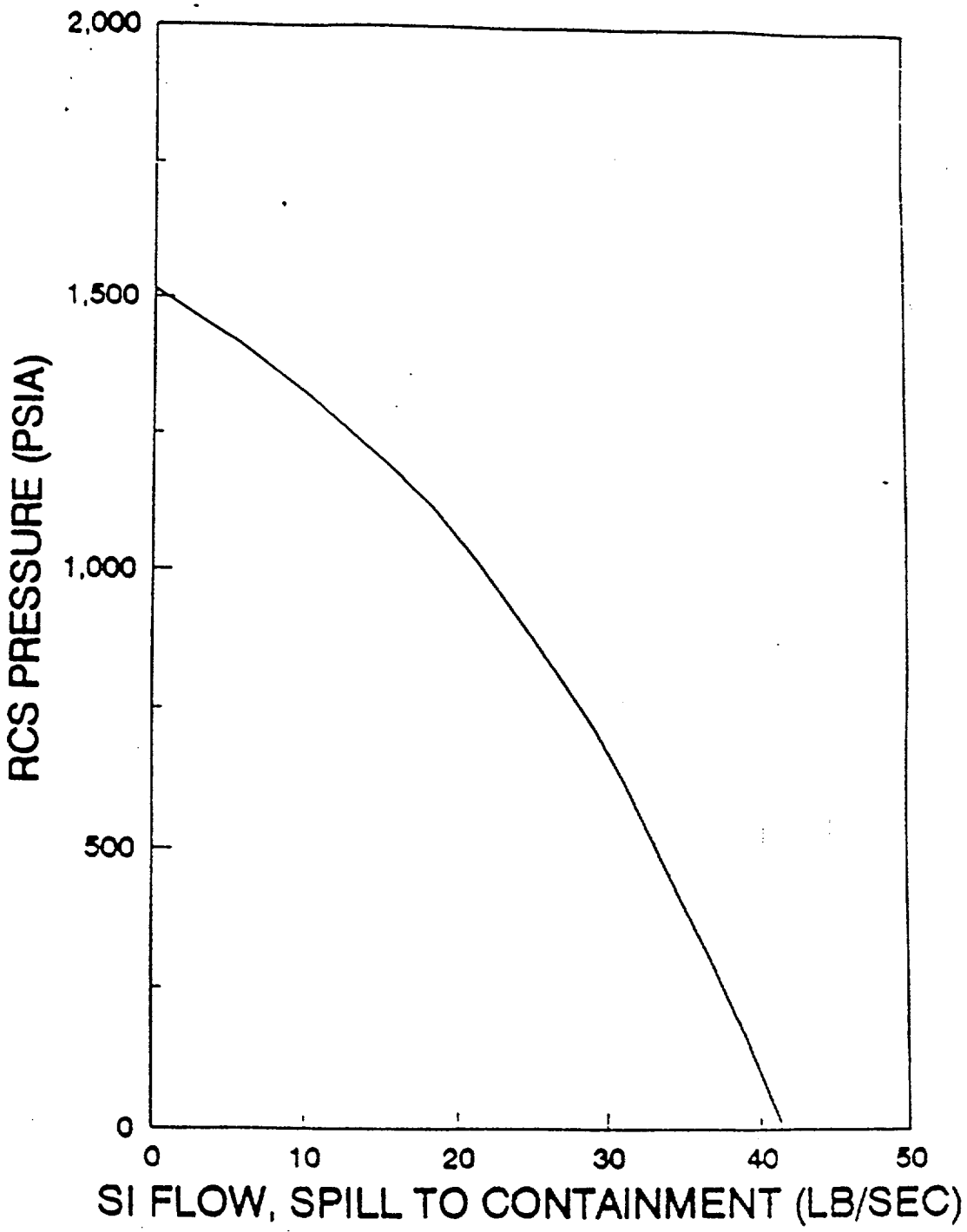

FIGURE 14.3-1a PUMPED SAFETY INJECTION FLOW
(NOTE: This plot contains an assumption for spilling to containment pressure)

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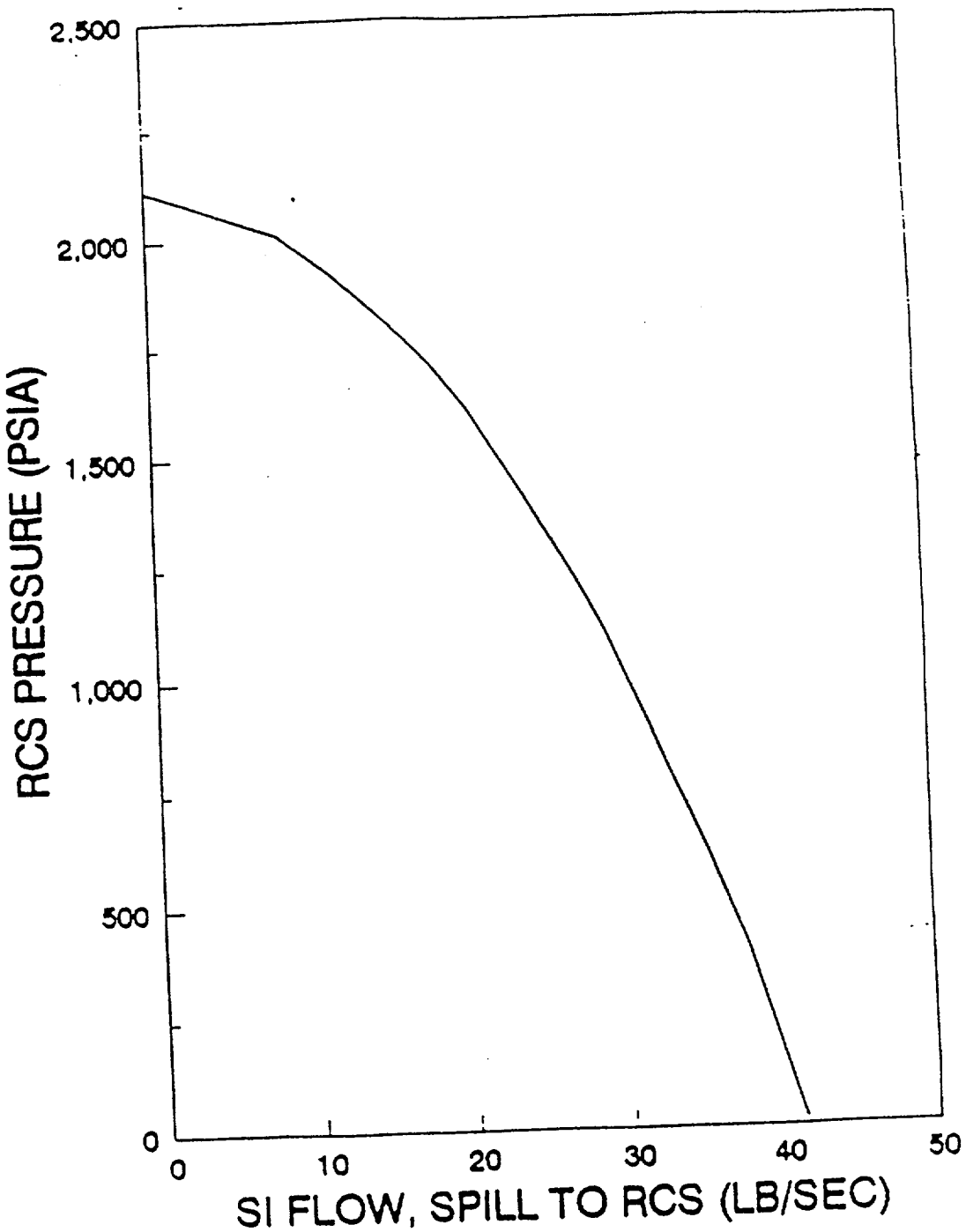

FIGURE 14.3-1b PUMPED SAFETY INJECTION FLOW
(NOTE: This plot contains an assumption for spilling to RCS pressure)

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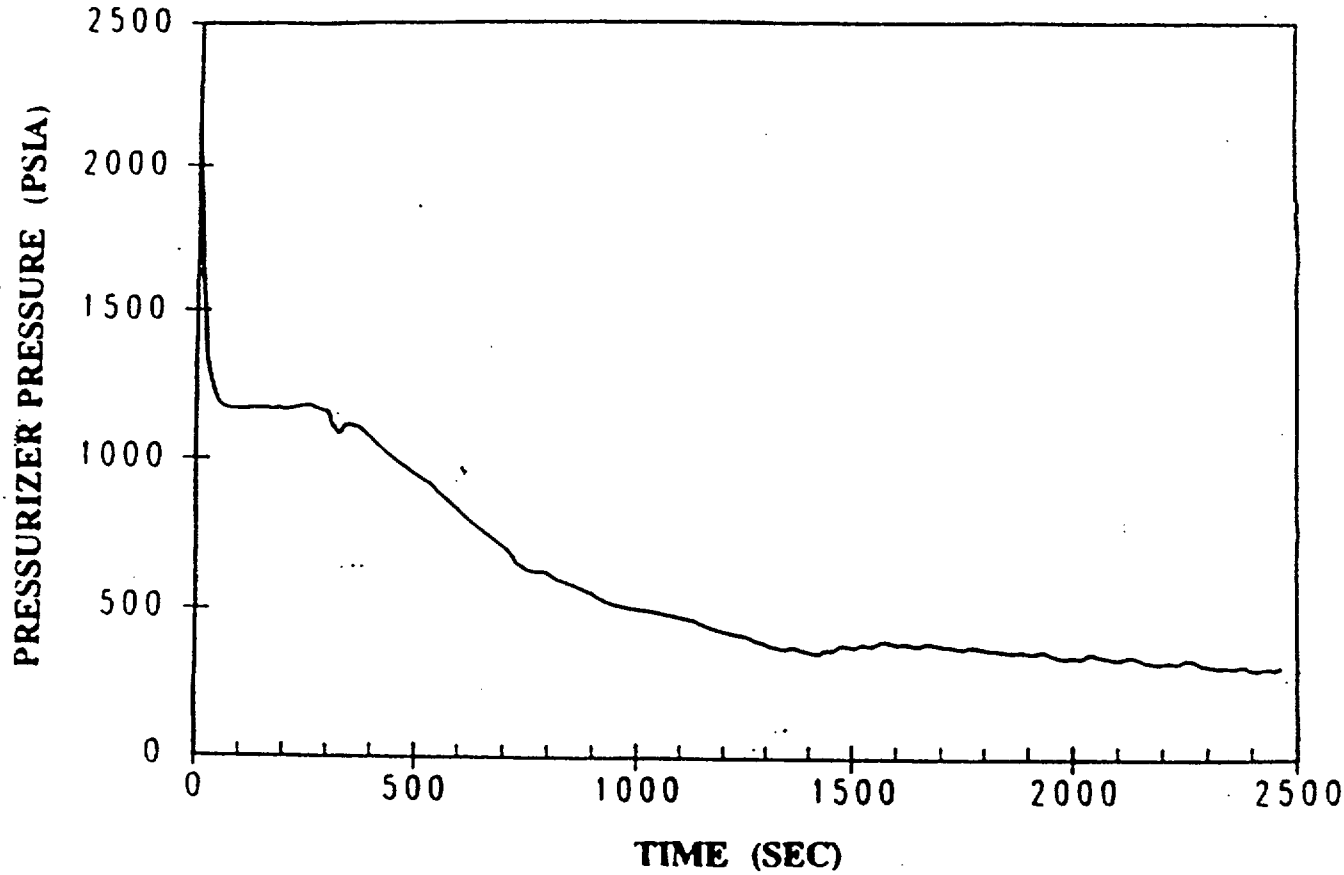

RCS DEPRESSURIZATION TRANSIENT (3 in)

FIGURE 14.3-1c

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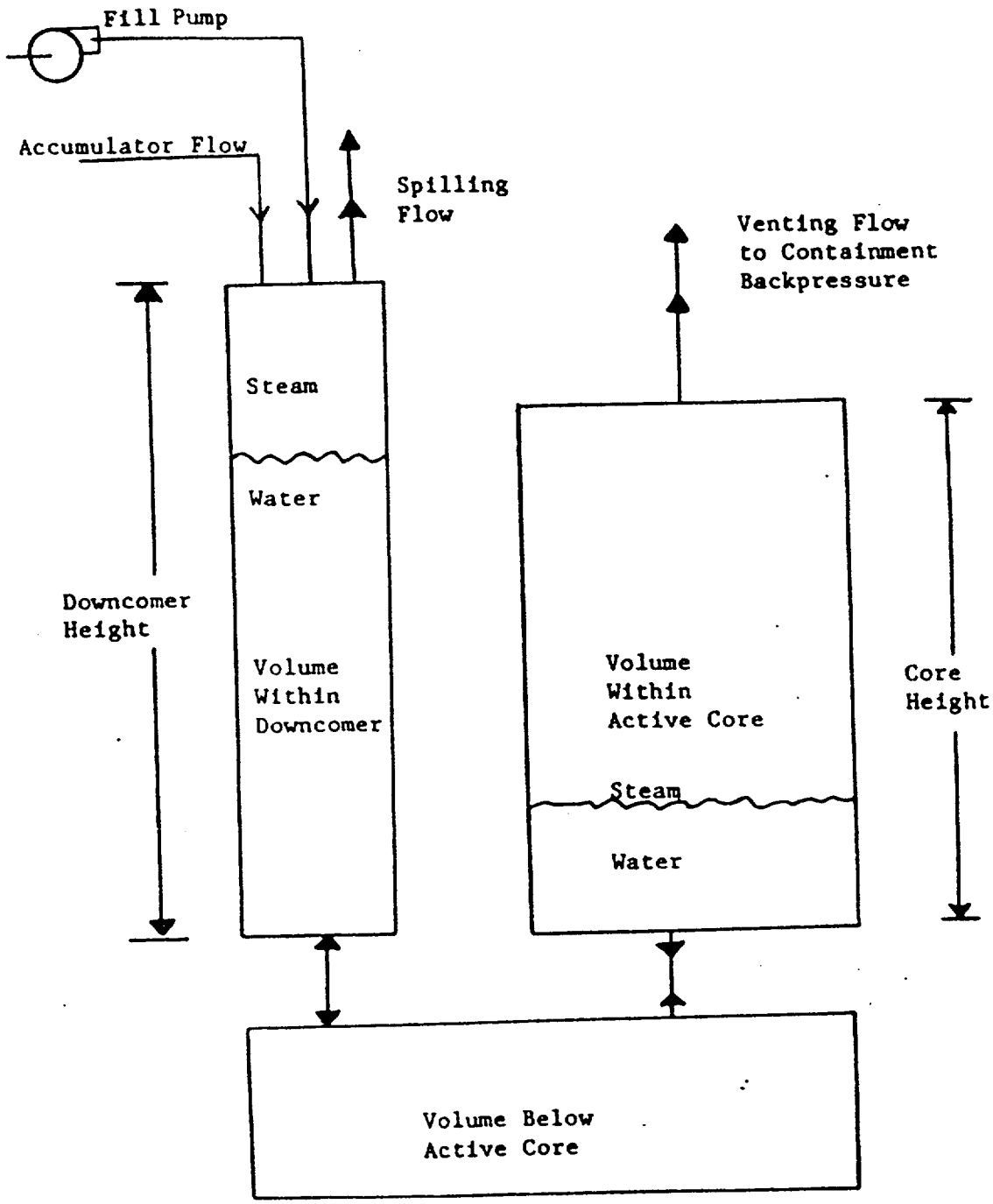

BLOCK DIAGRAM FOR SATAN-V REFILL CALCULATION

FIGURE 14.3-2

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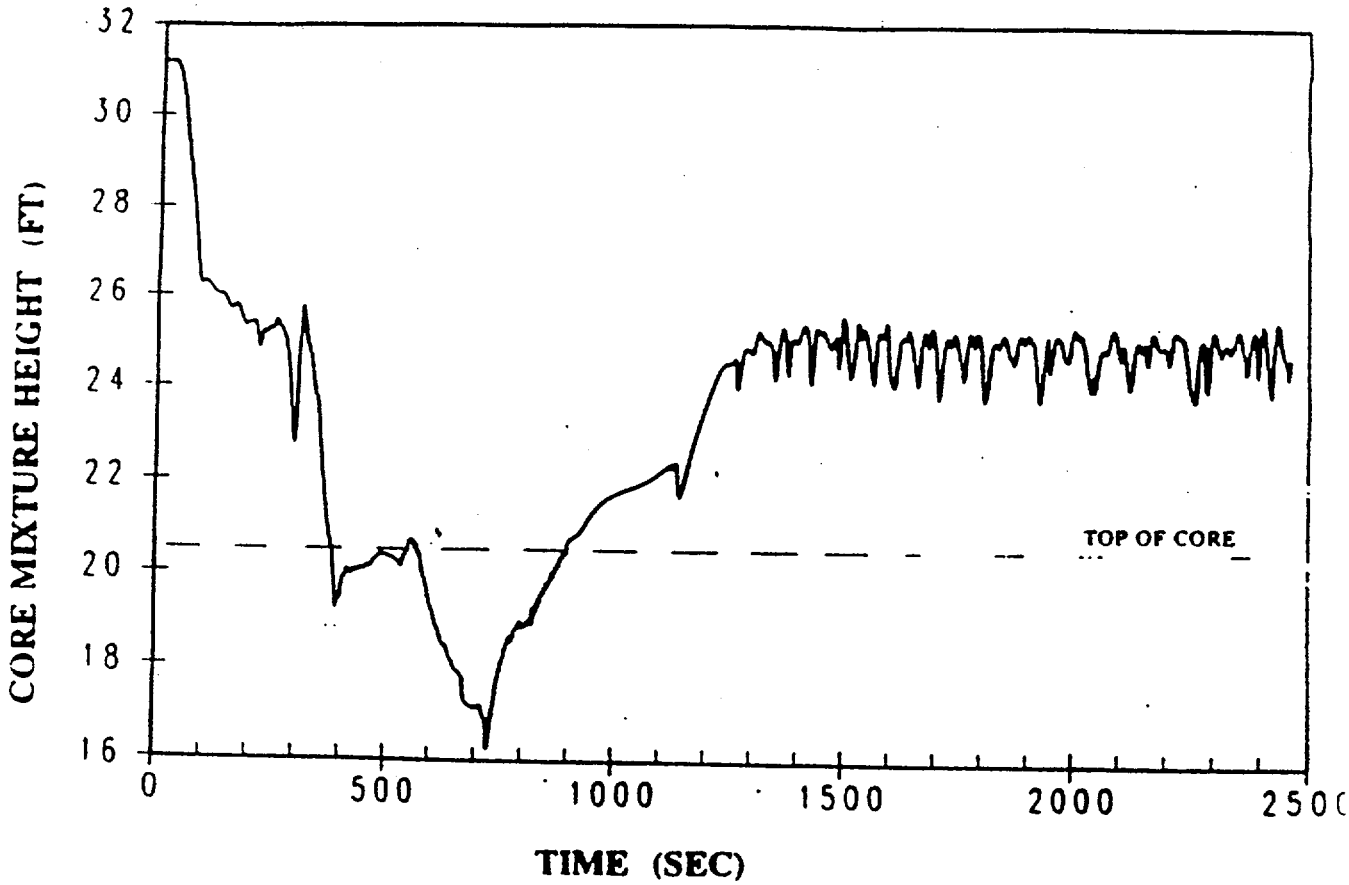

CORE MIXTURE HEIGHT (3 in)

FIGURE 14.3-2a

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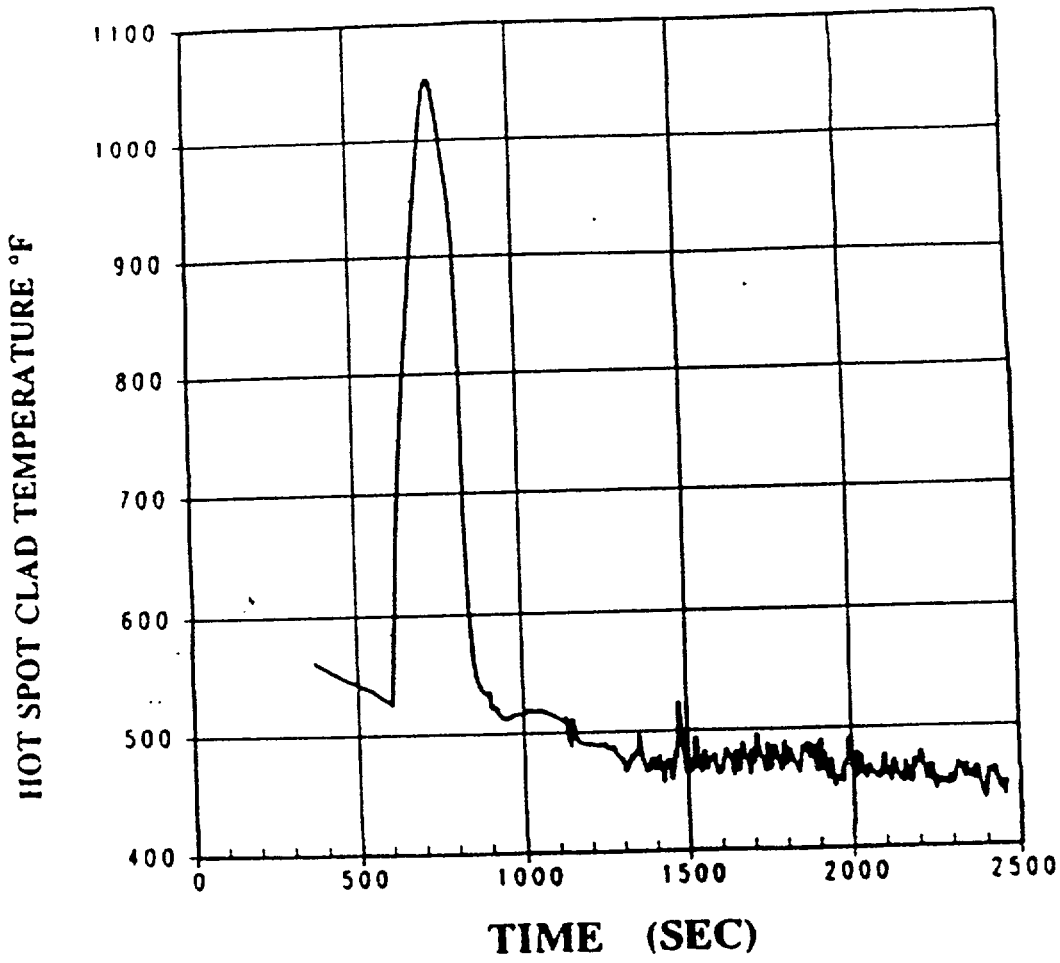

CLAD TEMPERATURES TRANSIENT (3 in)

FIGURE 14.3-2b

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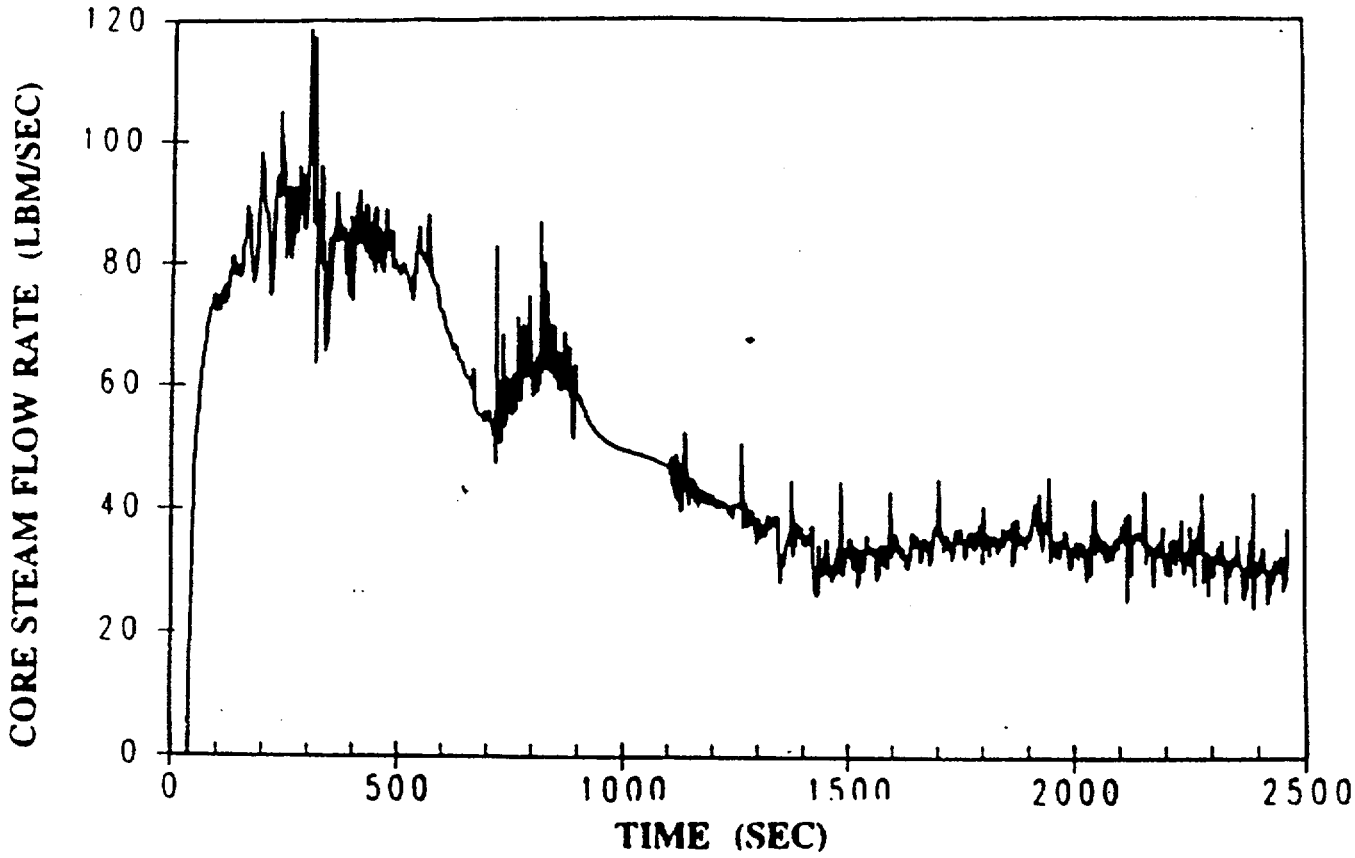

STEAM FLOW (3 in)

FIGURE 14.3-3a

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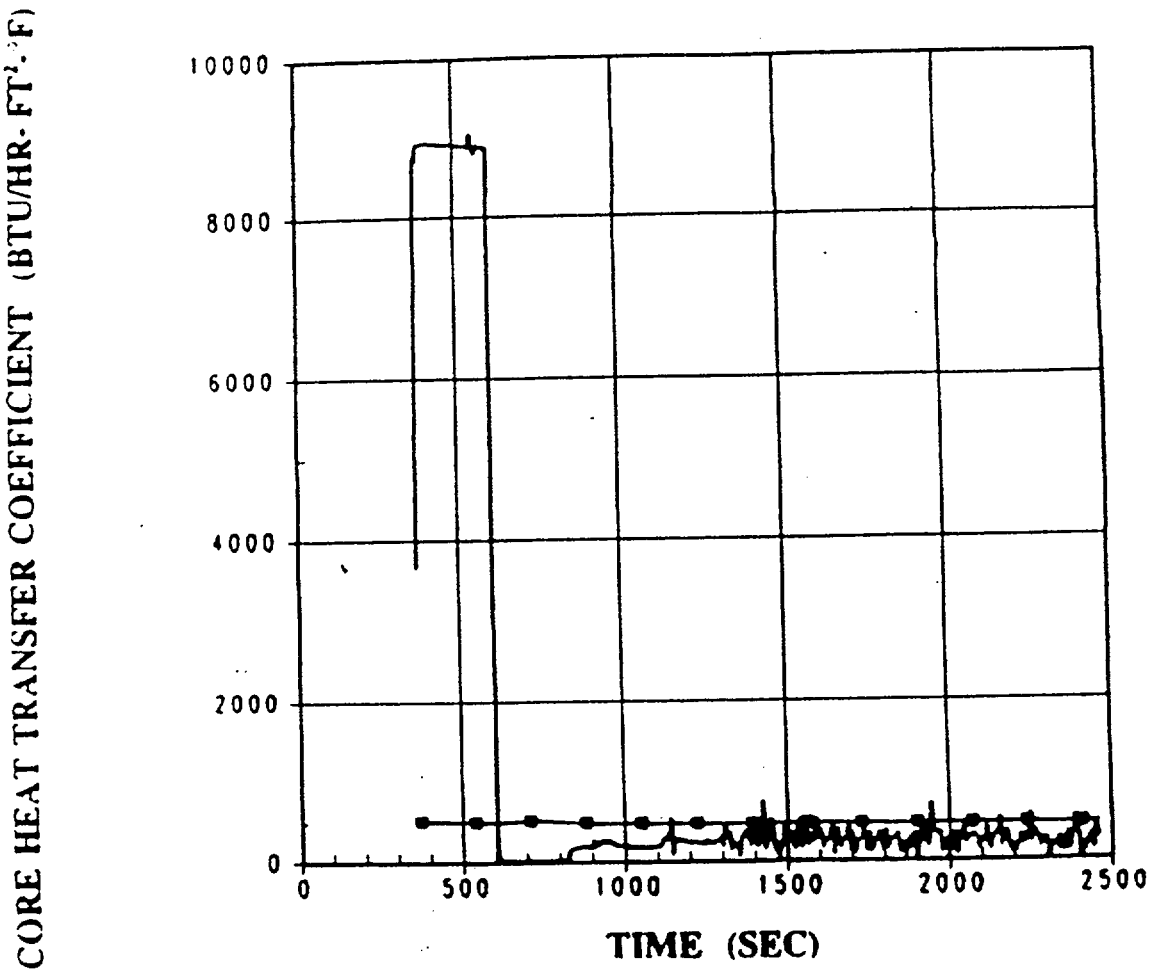

CORE HEAT TRANSFER COEFFICIENT (3 in)

FIGURE 14.3-3b

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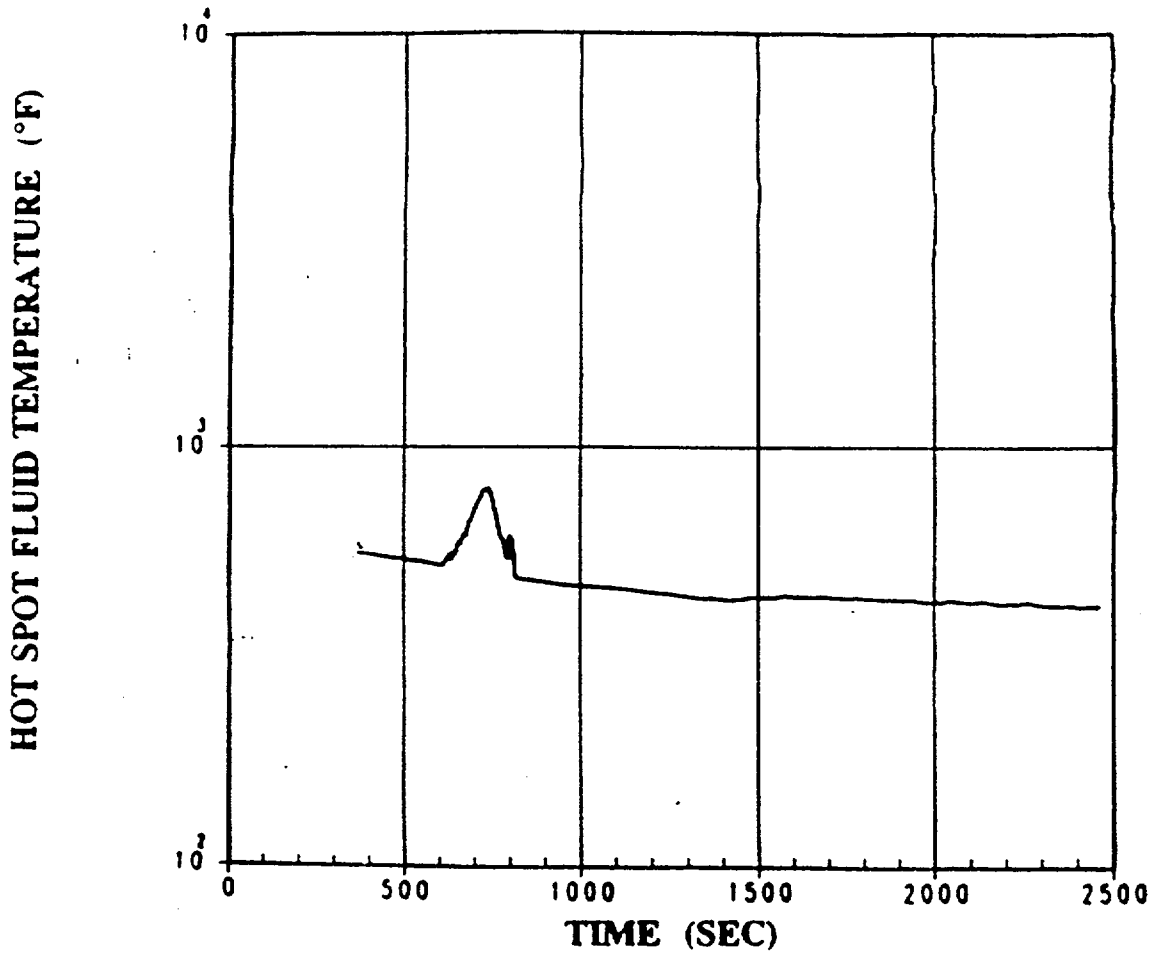

HOT SPOT FLUID TEMPERATURE (3 in)

FIGURE 14.3-4a

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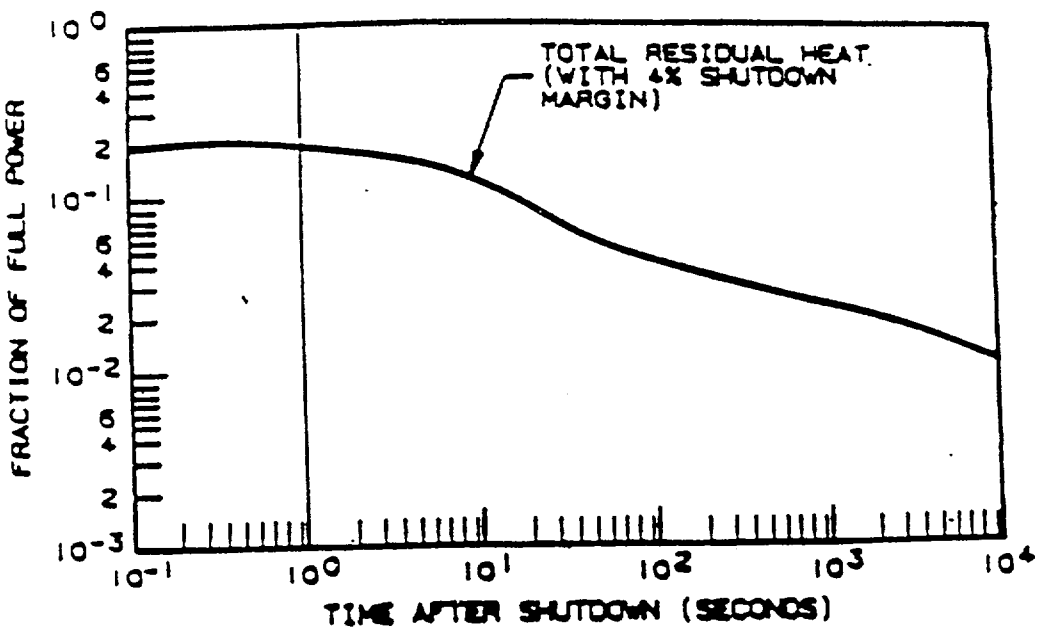

CORE POWER

FIGURE 14.3-4b

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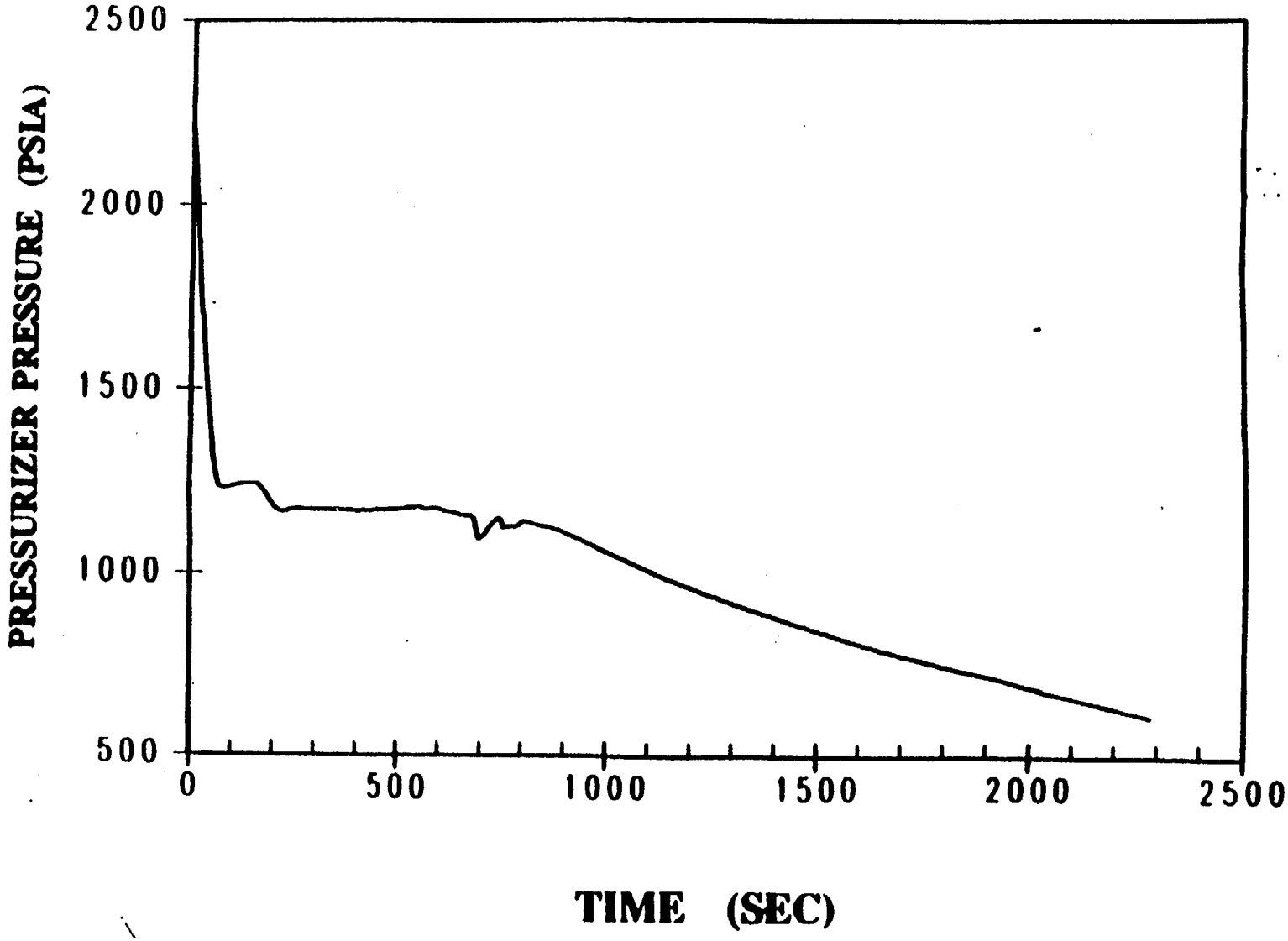

RCS DEPRESSURIZATION TRANSIENT (2 in)

FIGURE 14.3-5a

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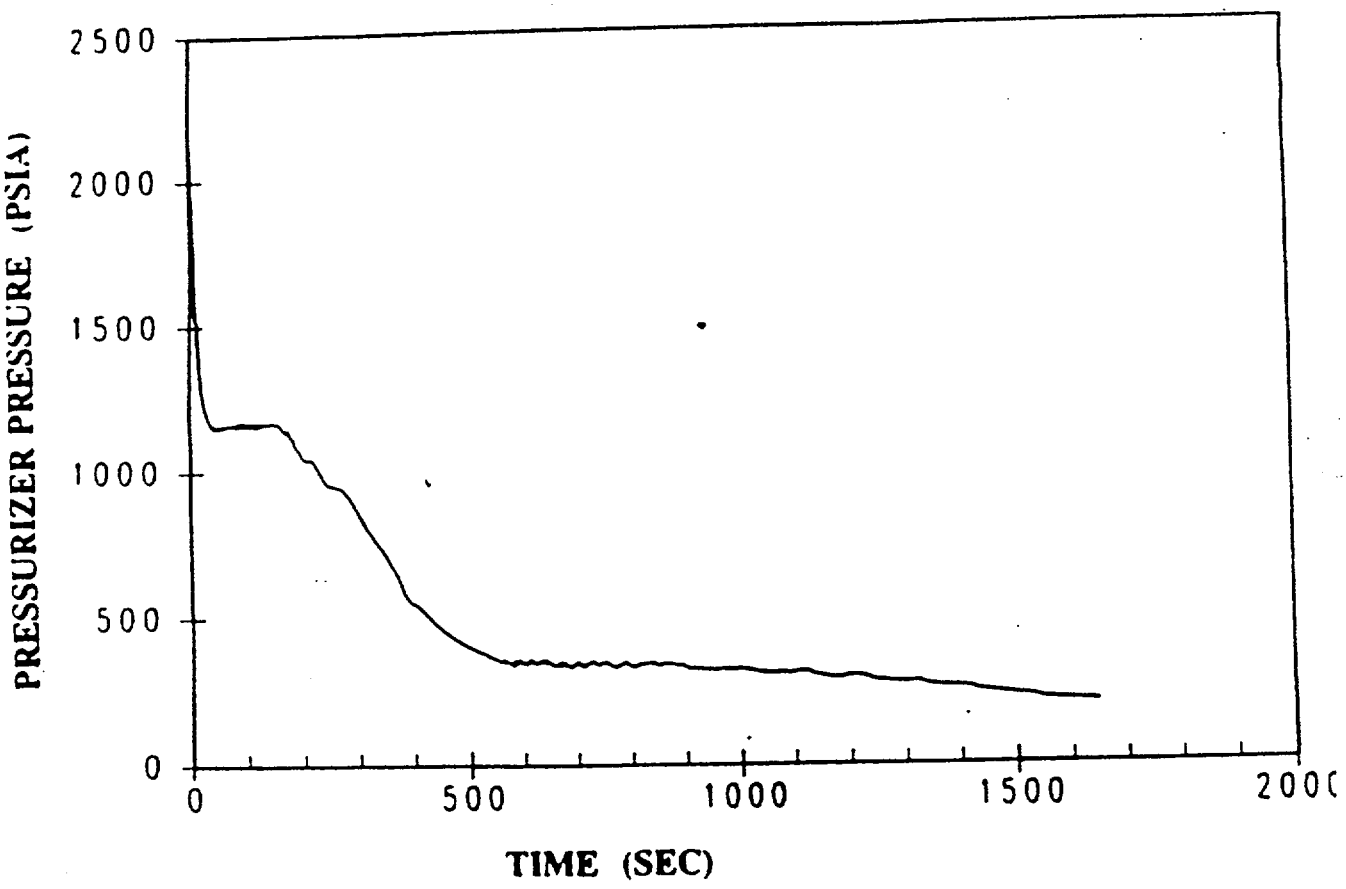

RCS DEPRESSURIZATION TRANSIENT (4 in)

FIGURE 14.3-5b

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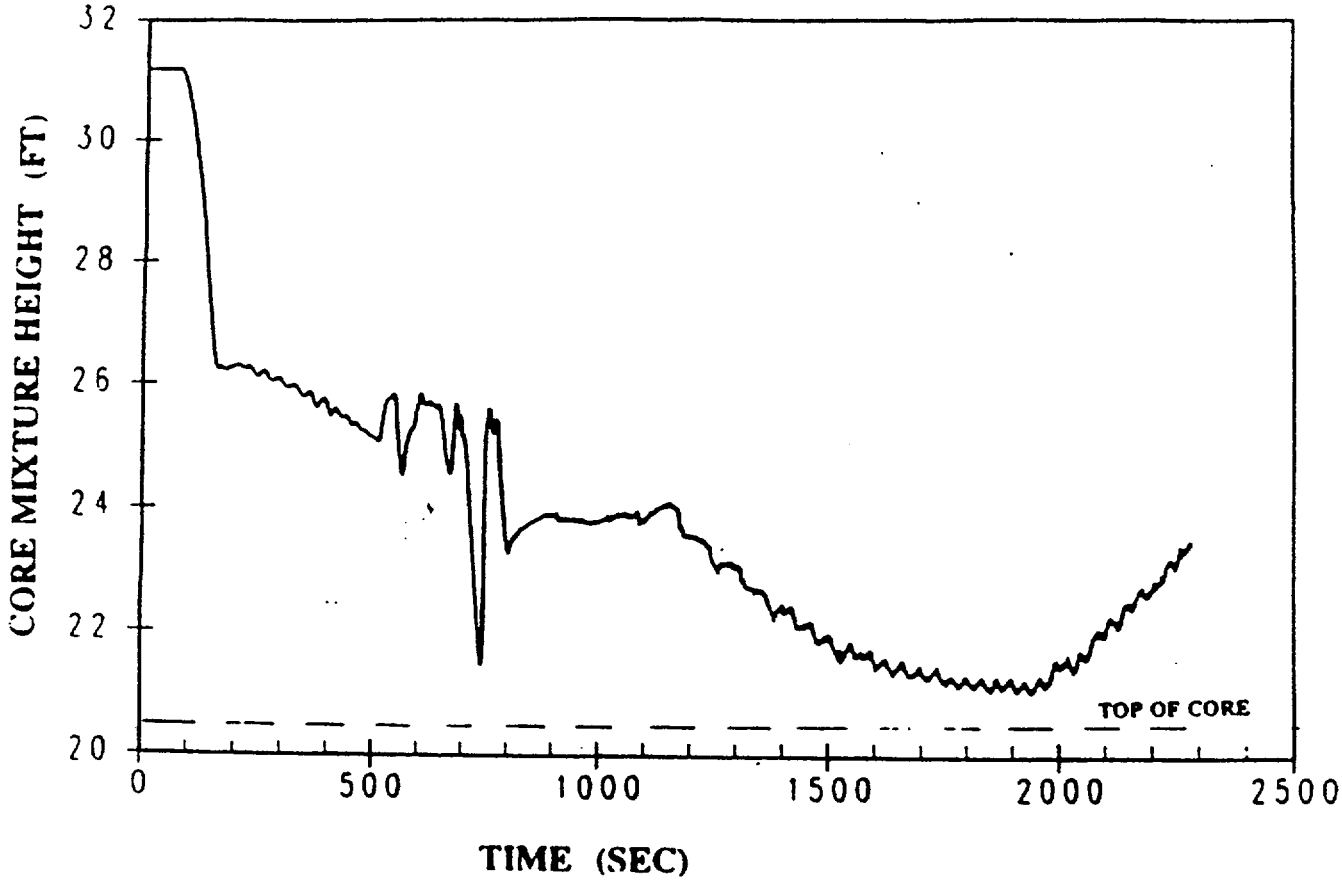

CORE MIXTURE HEIGHT (2 in)

FIGURE 14.3-6a

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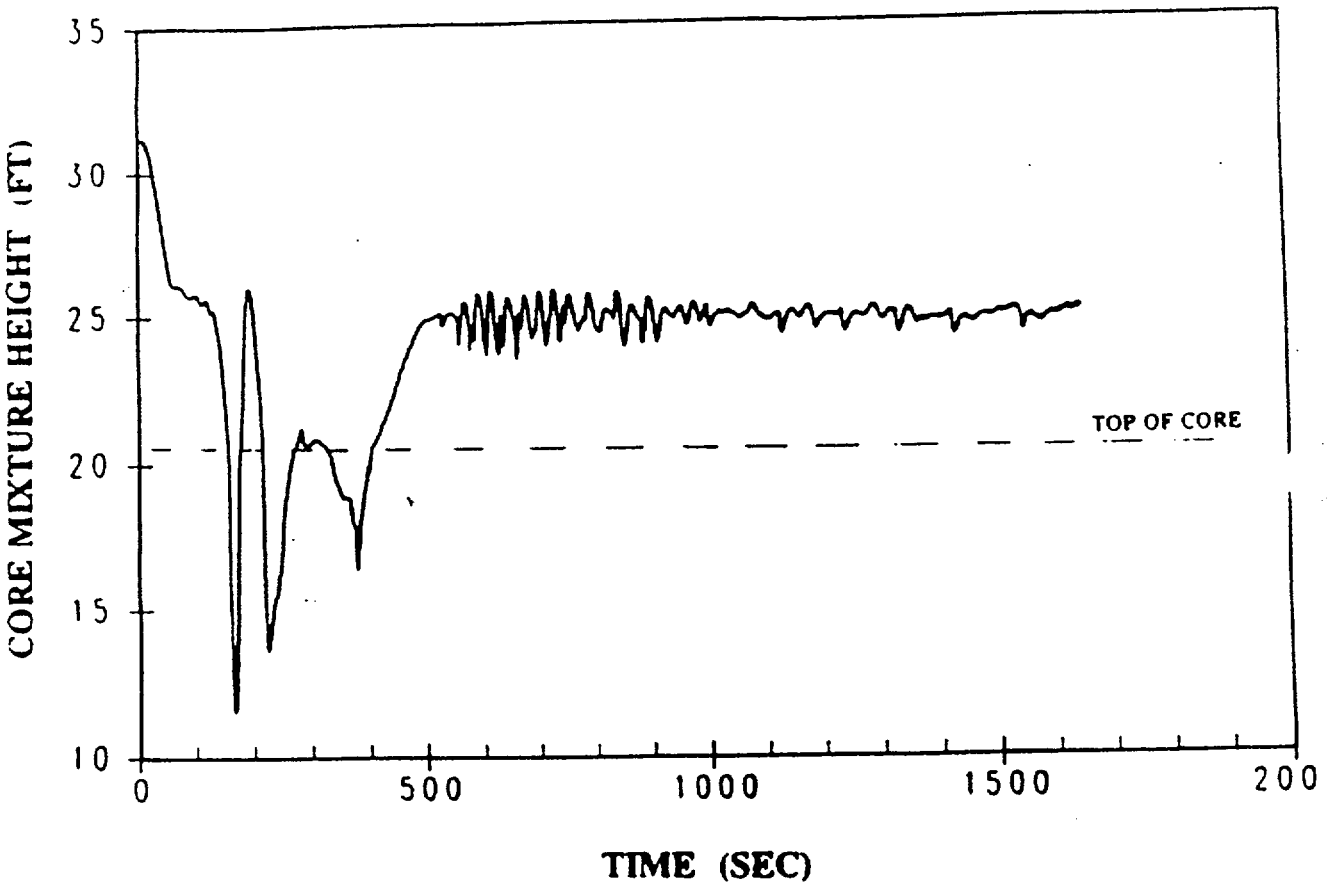

CORE MIXTURE HEIGHT (4 in)

FIGURE 14.3-6b

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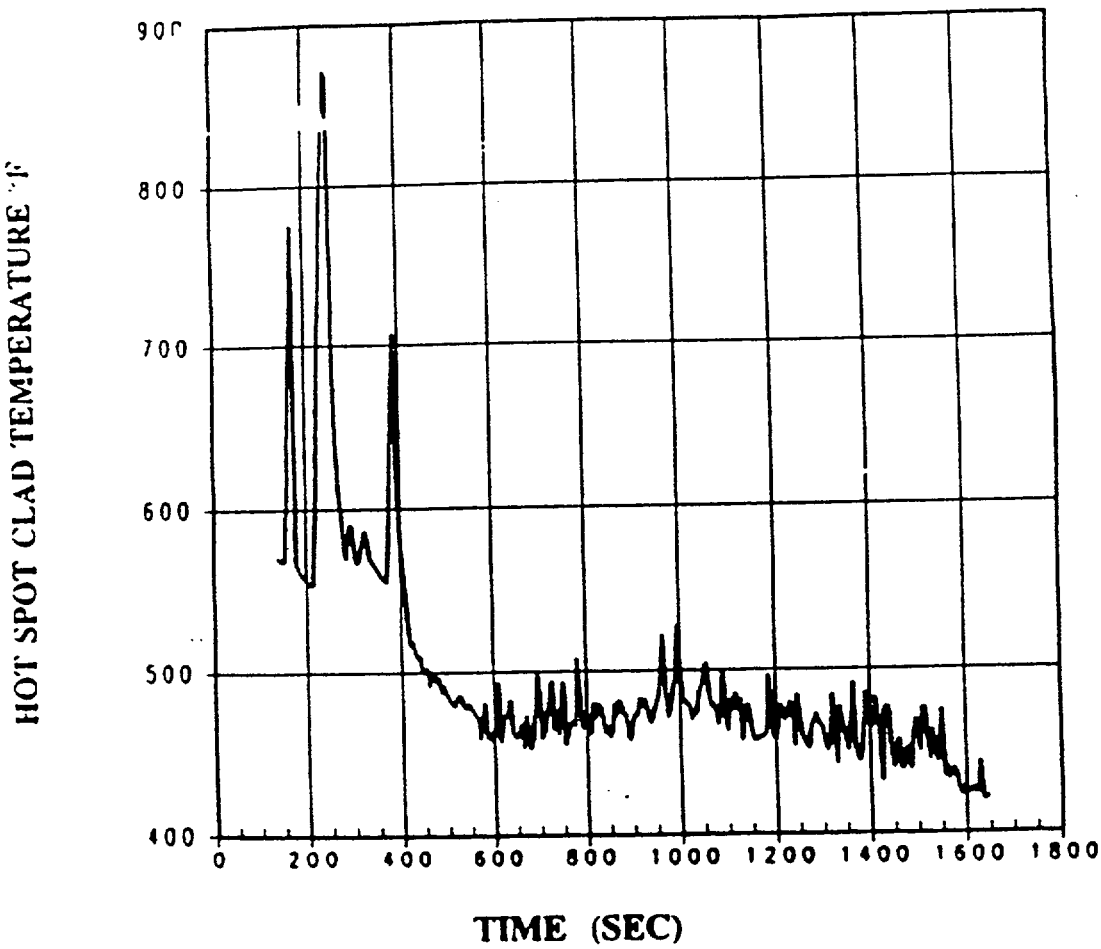

CLAD TEMPERATURE TRANSIENT (4 in)

FIGURE 14.3-7b

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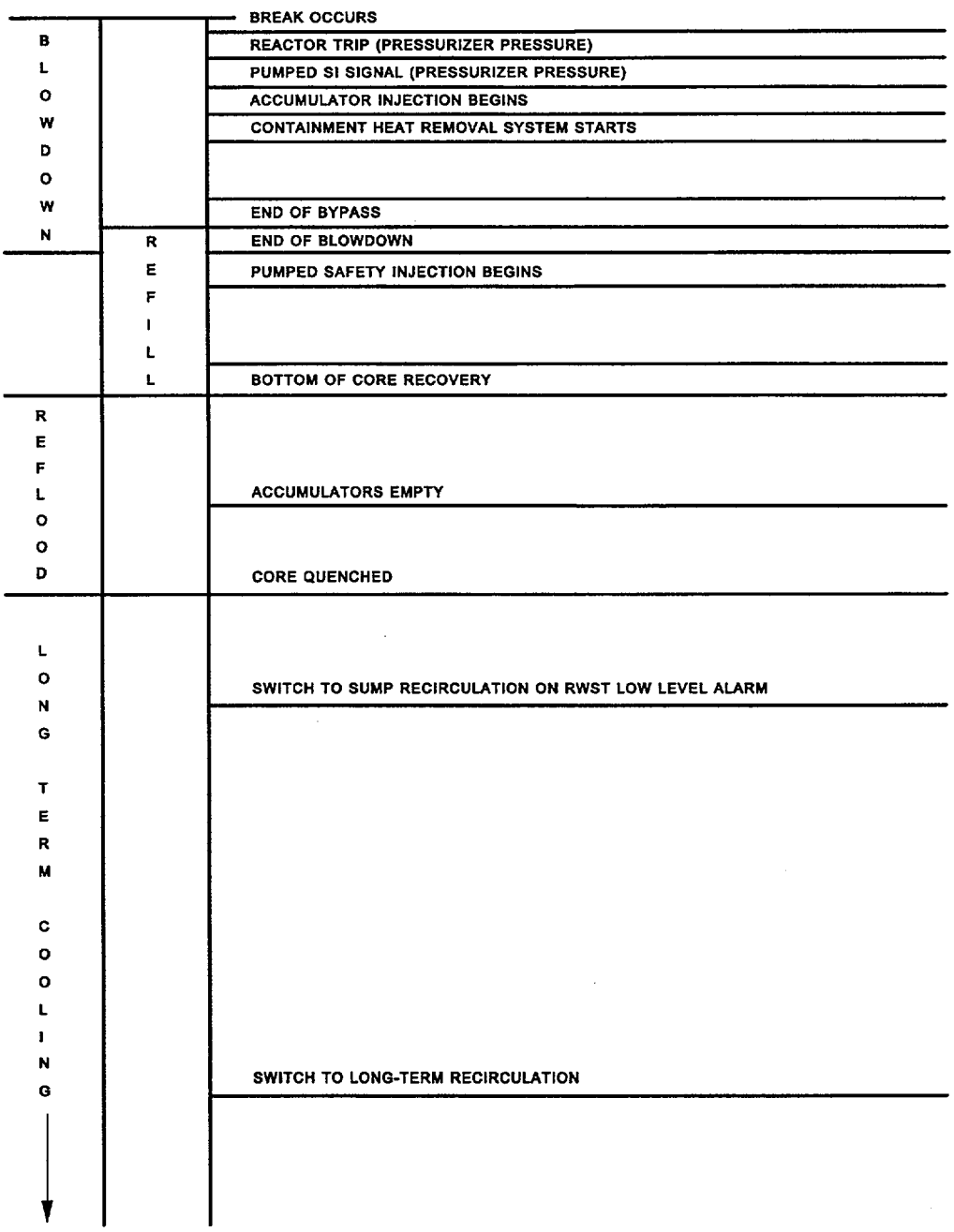

Large Break LOCA Sequence of Events
Figure 14.3.2-1

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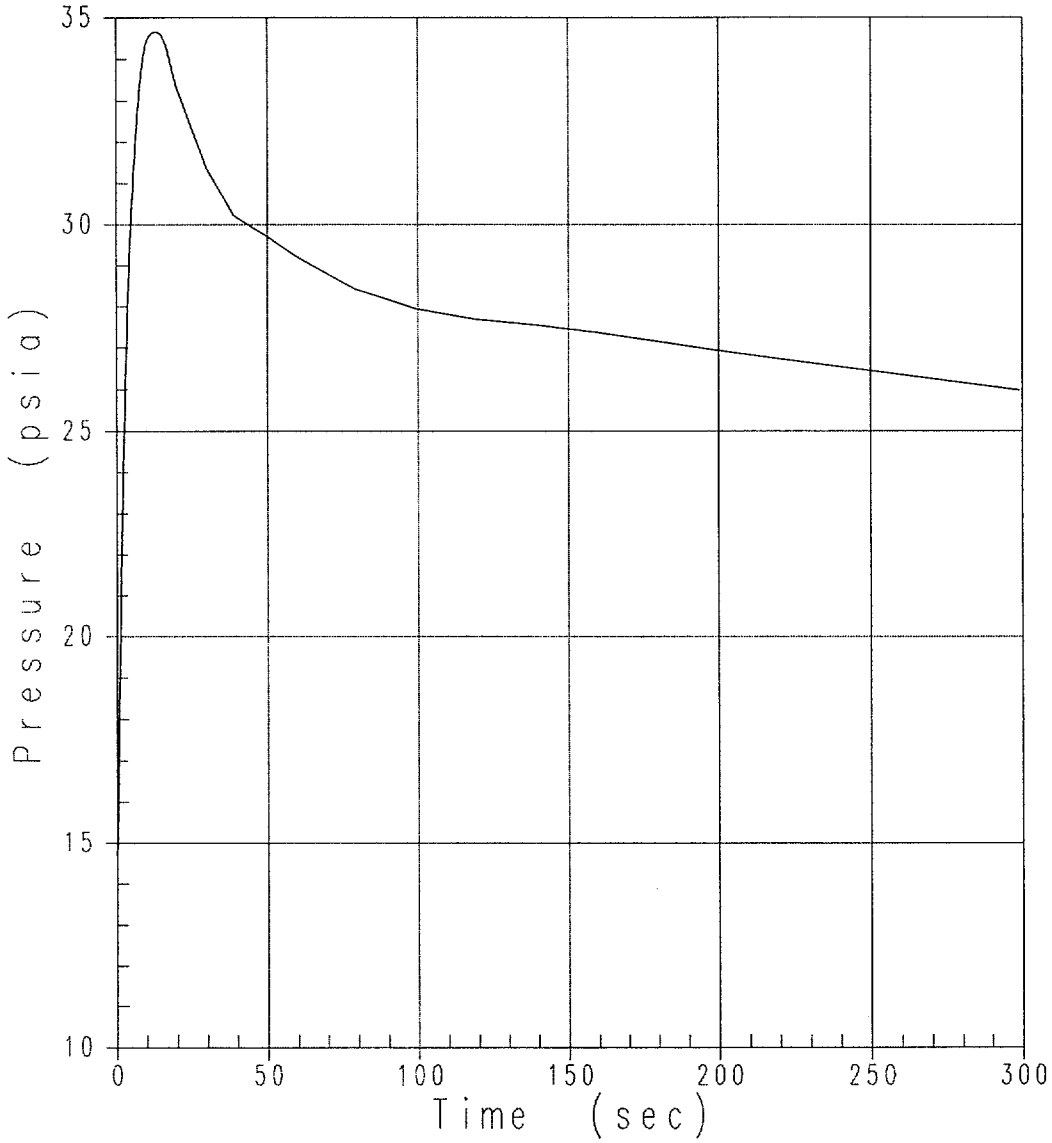

Large Break LOCA Containment Pressure Curve
Figure 14.3.2-2

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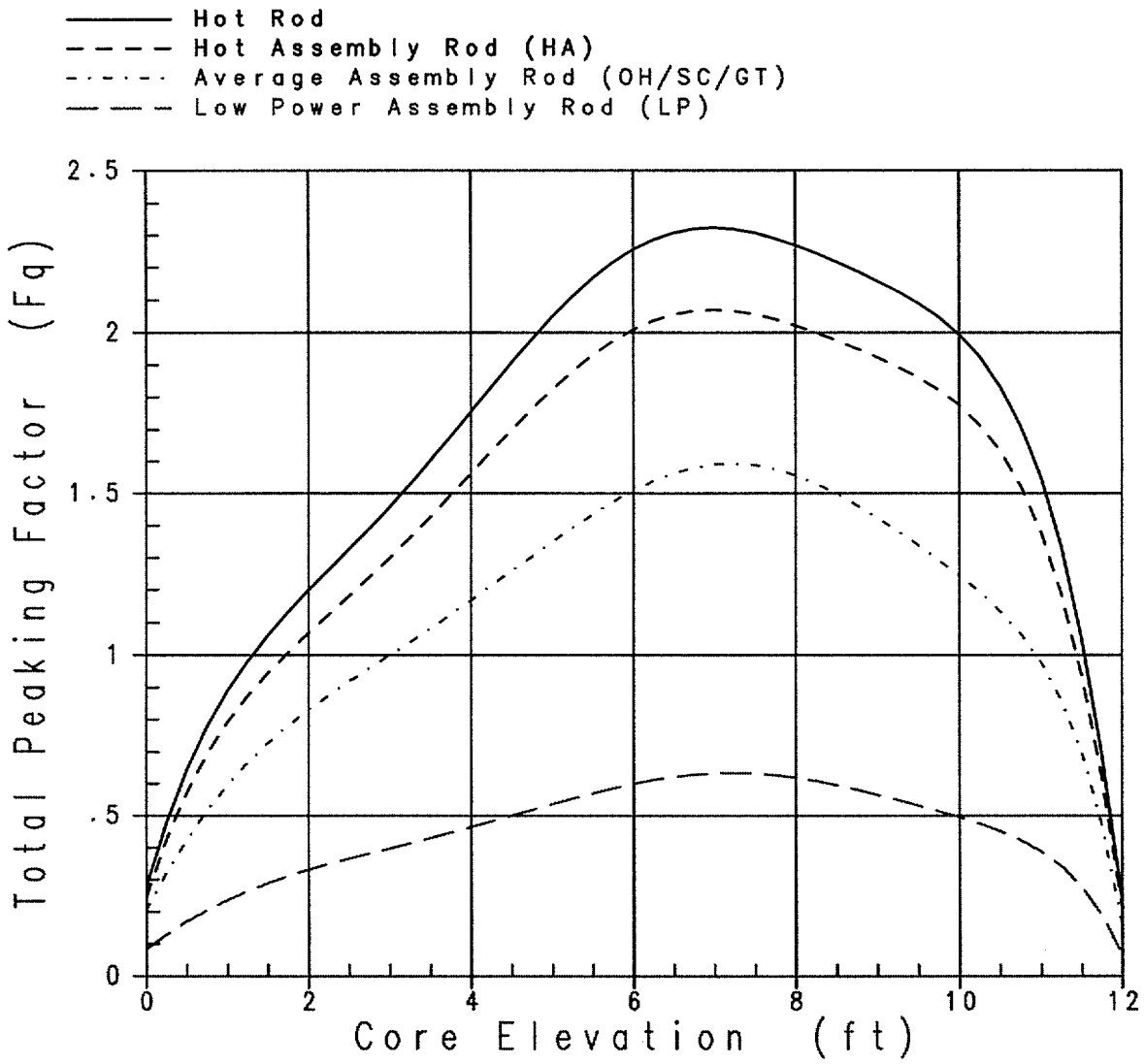
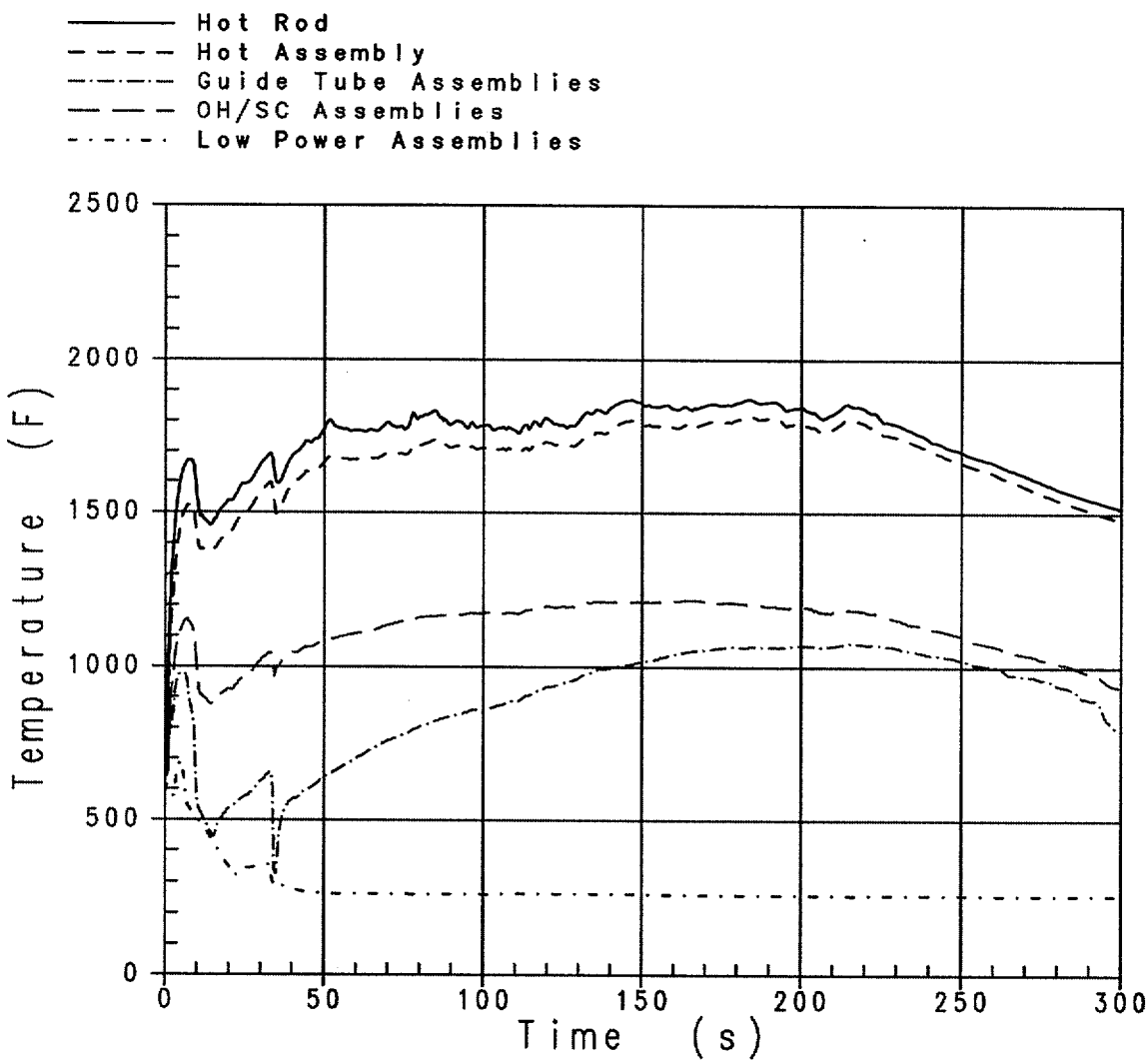
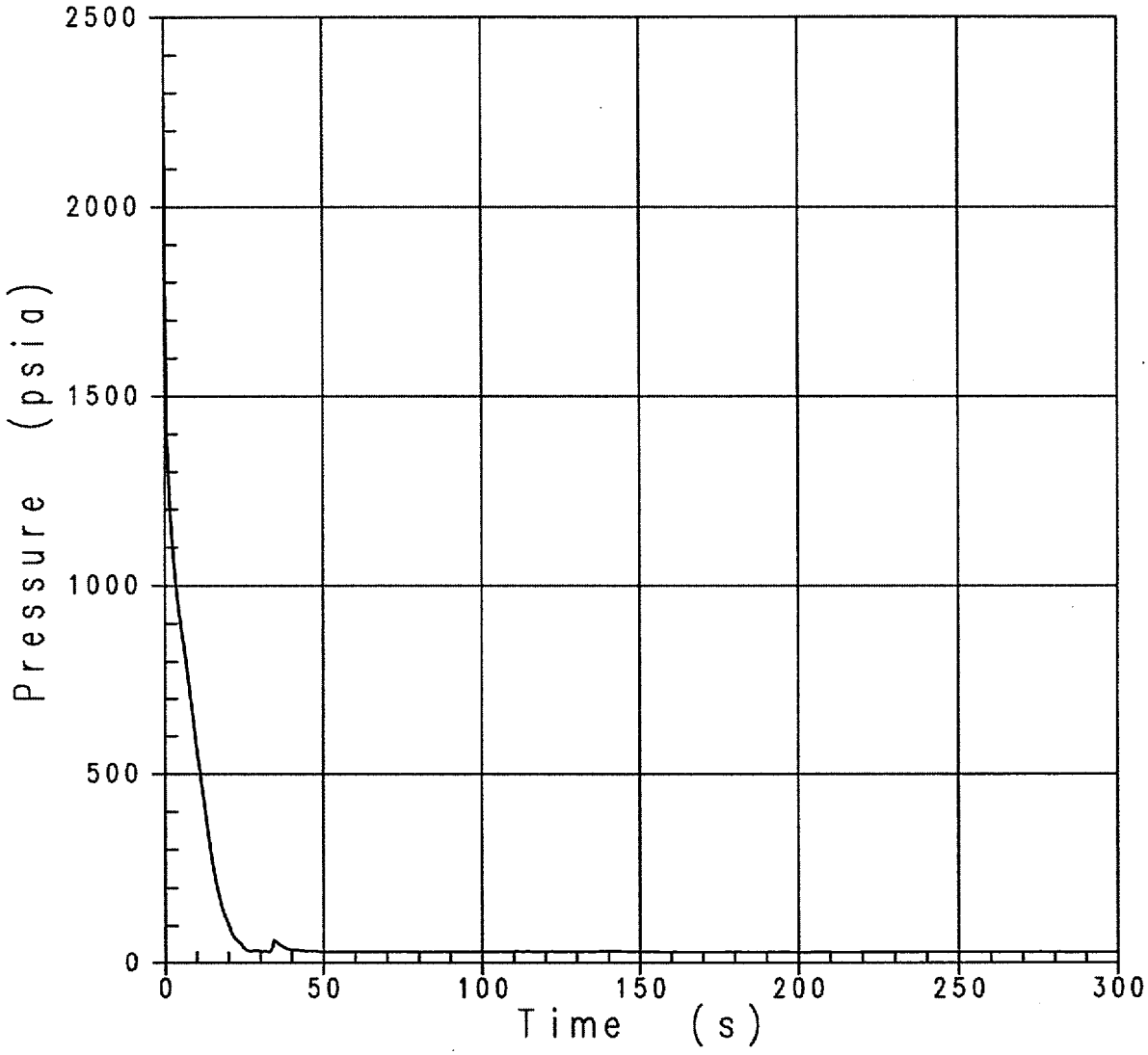


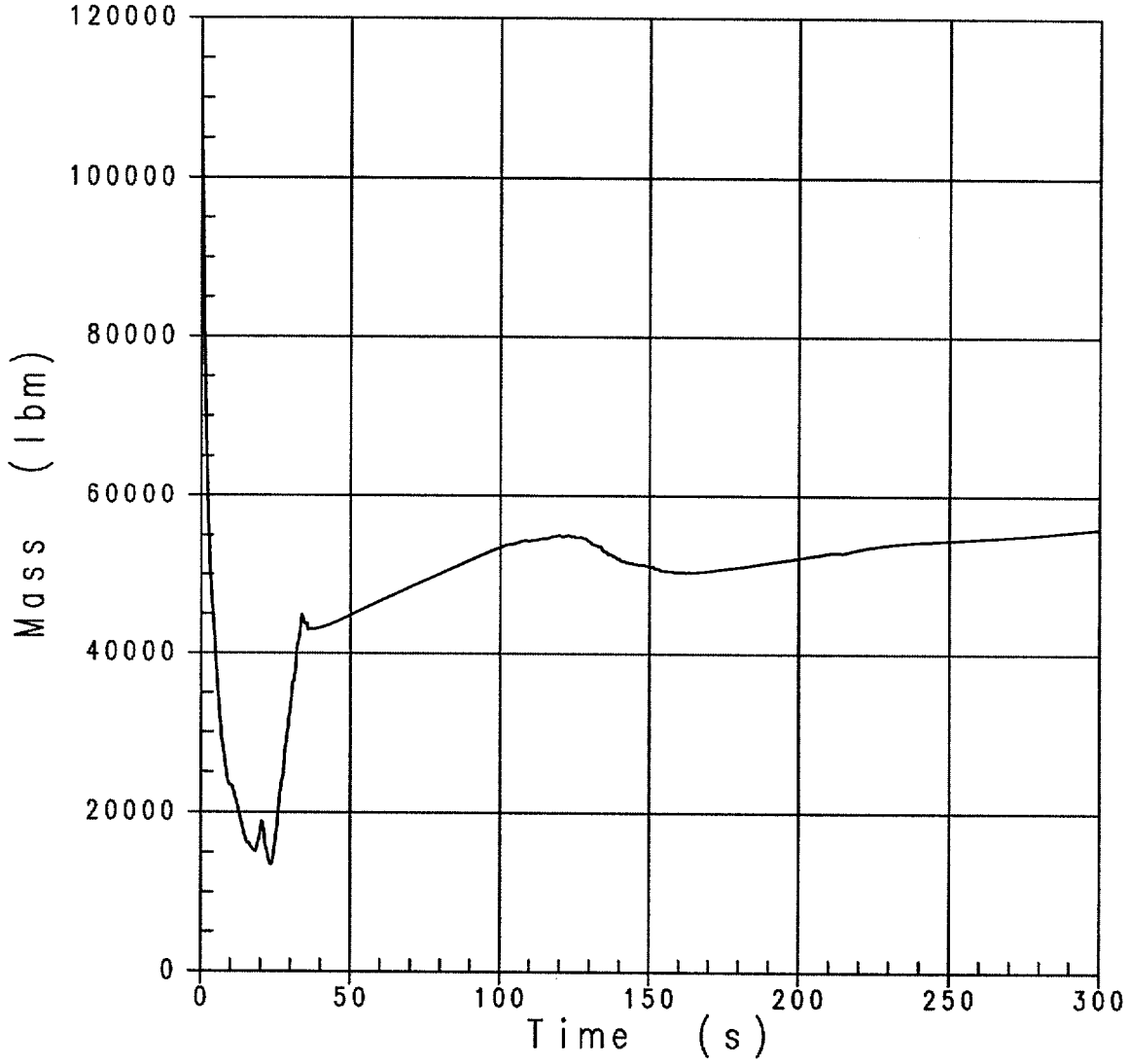
Large Break LOCA Appendix K Power Shape
Figure 14.3.2-3



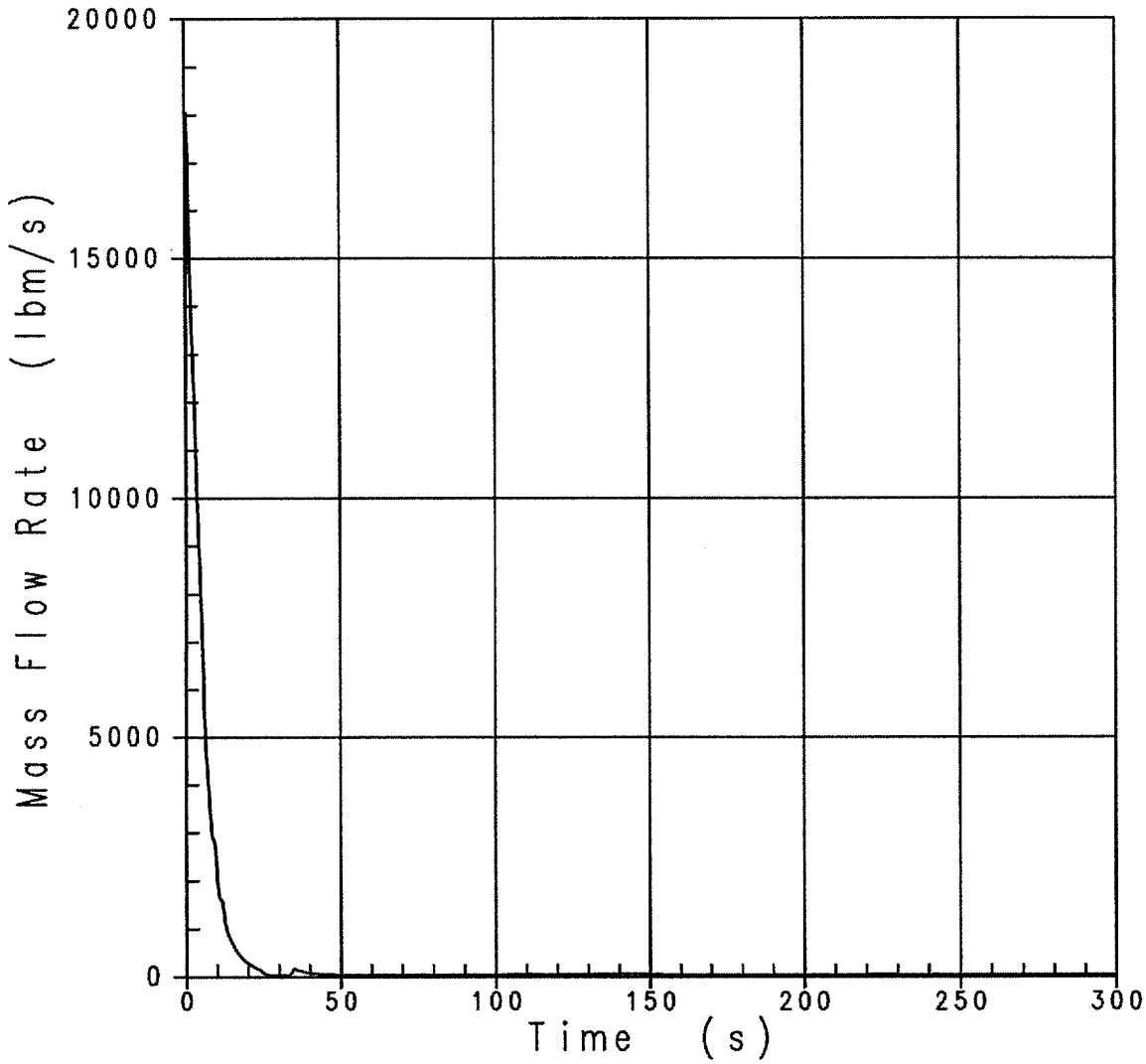
Appendix K Calculation
Peak Cladding Temperature
Figure 14.3.2-4



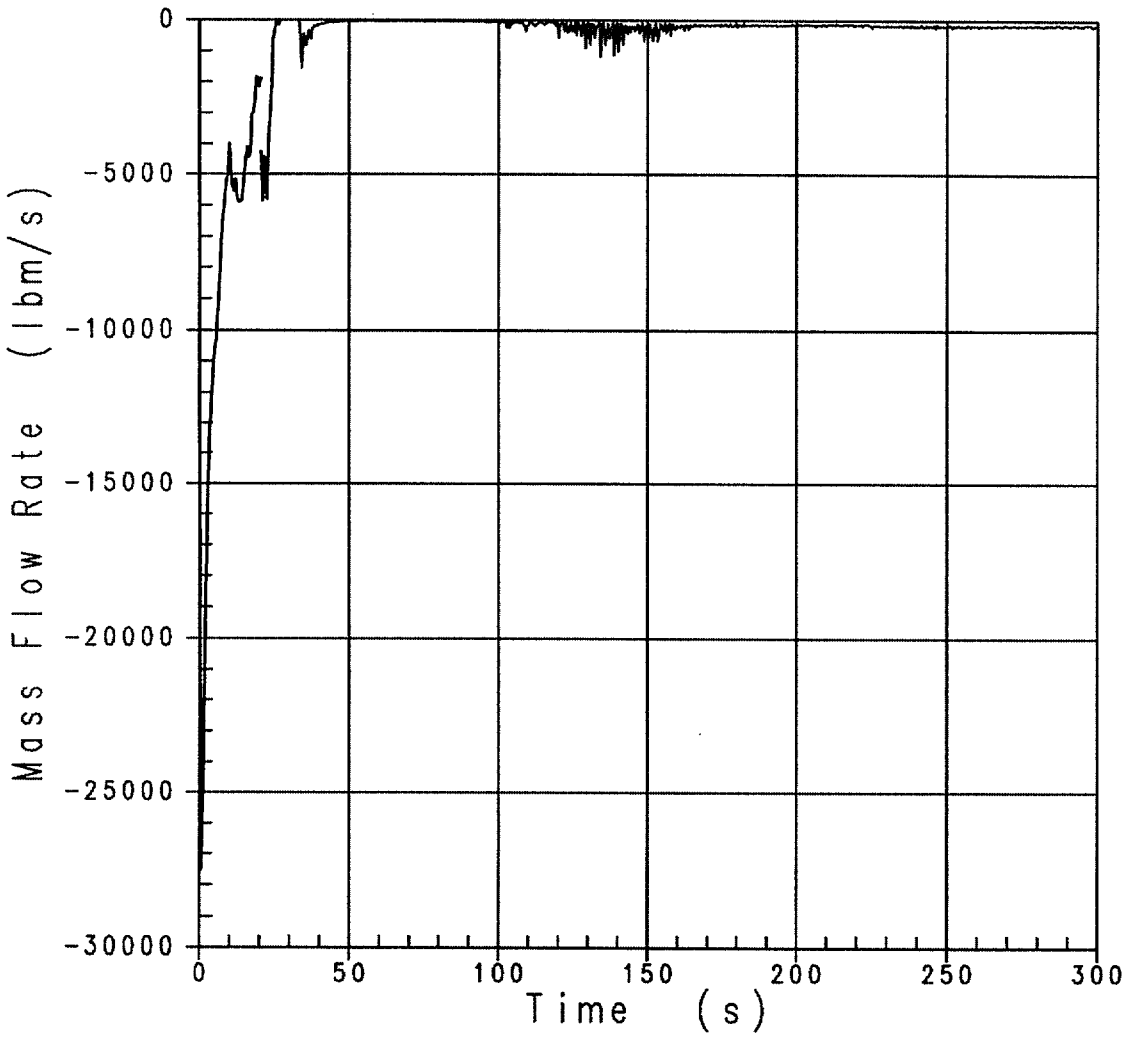
Appendix K Calculation
Core Pressure
Figure 14.3.2-5



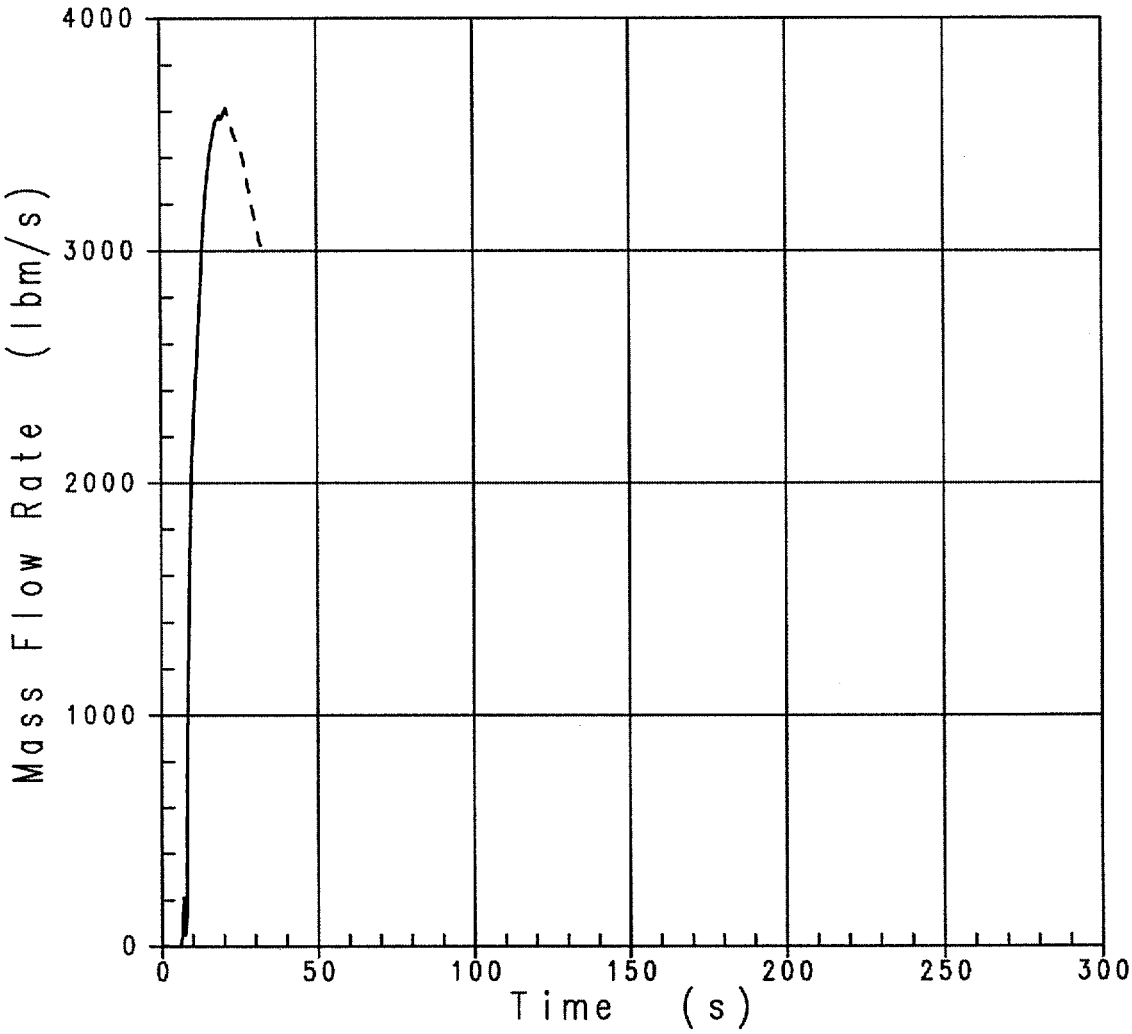
Appendix K Calculation
Vessel Inventory
Figure 14.3.2-6



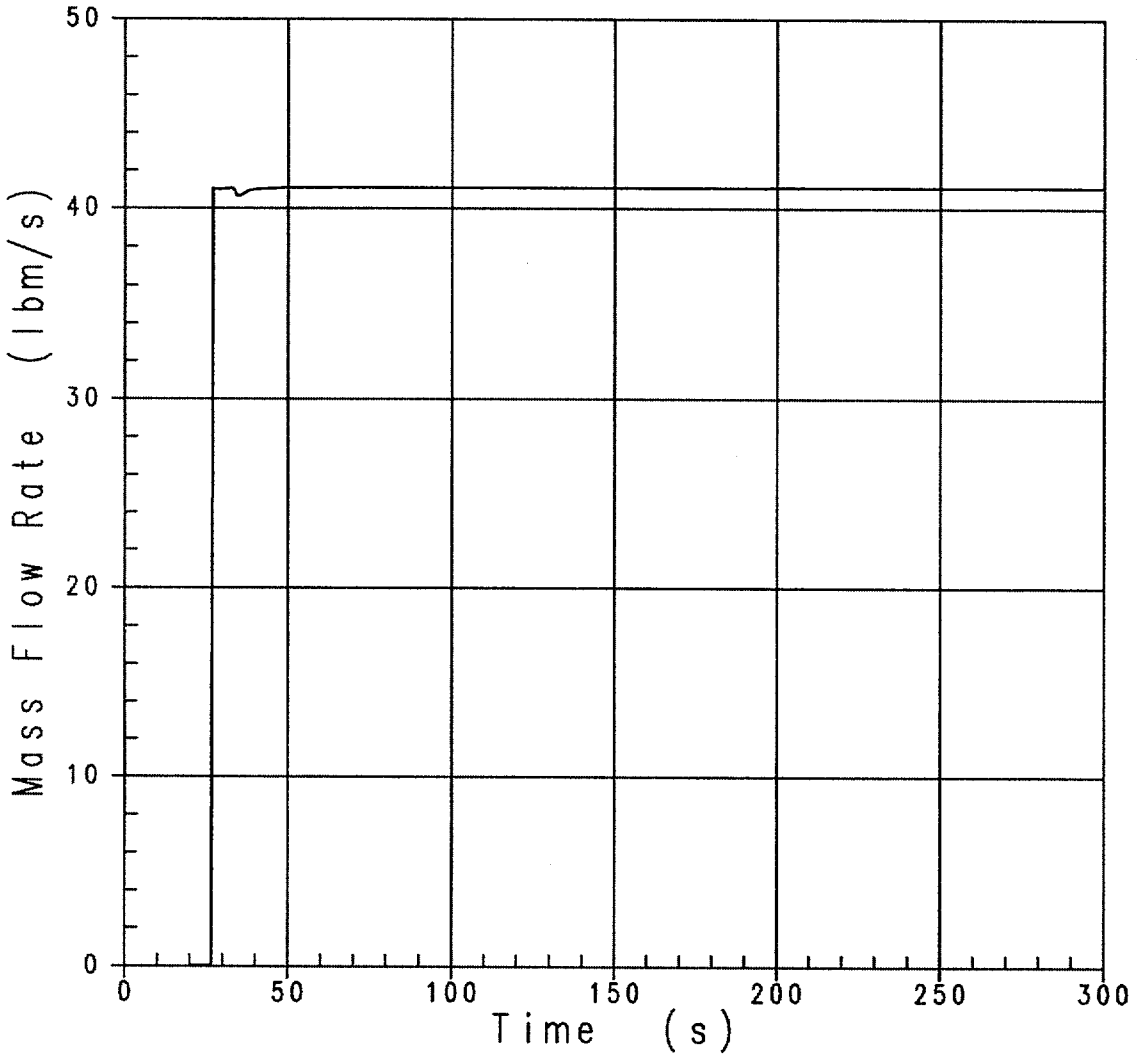
Appendix K Calculation
Loop Side Break Flow
Figure 14.3.2-7



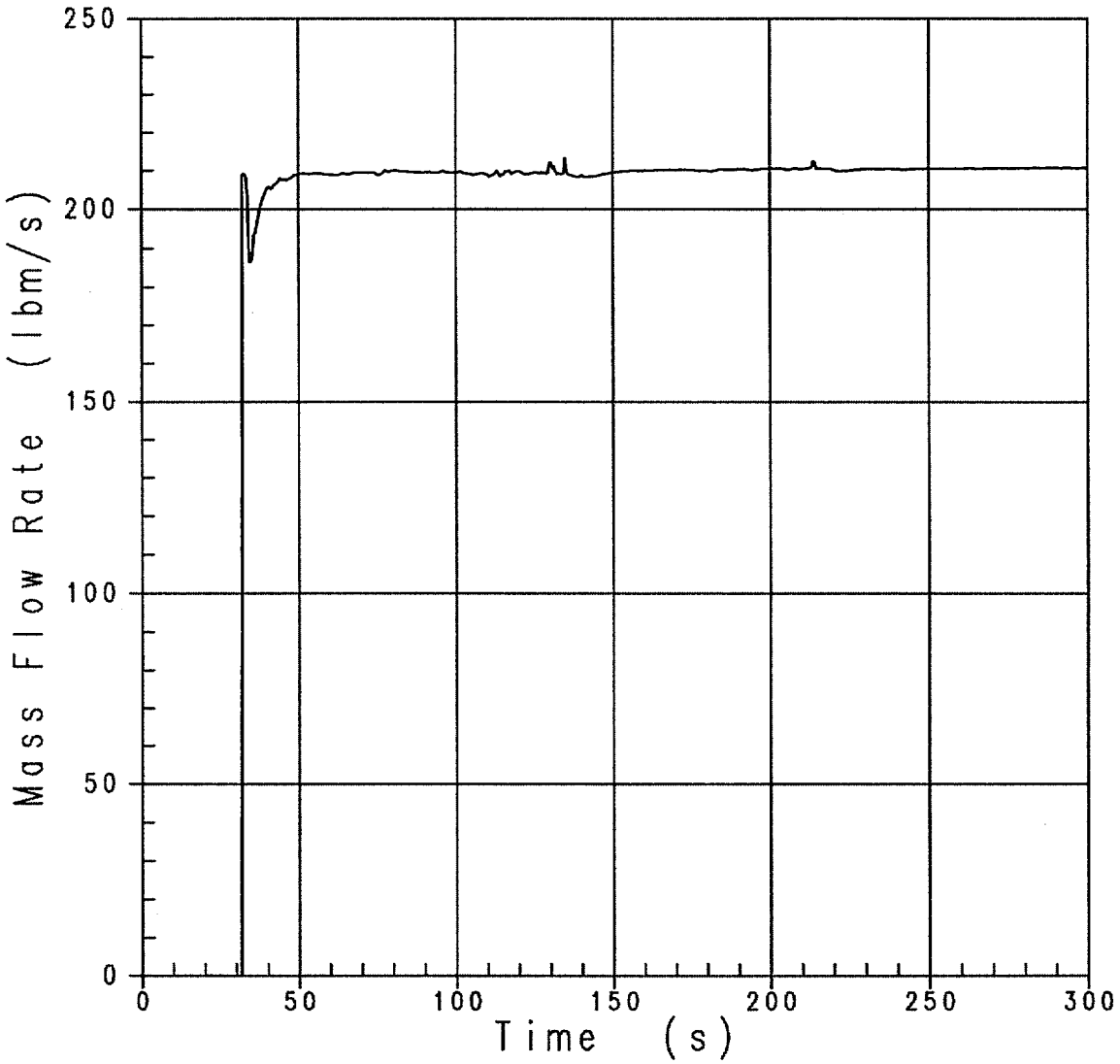
Appendix K Calculation
Vessel Side Break Flow
Figure 14.3.2-8



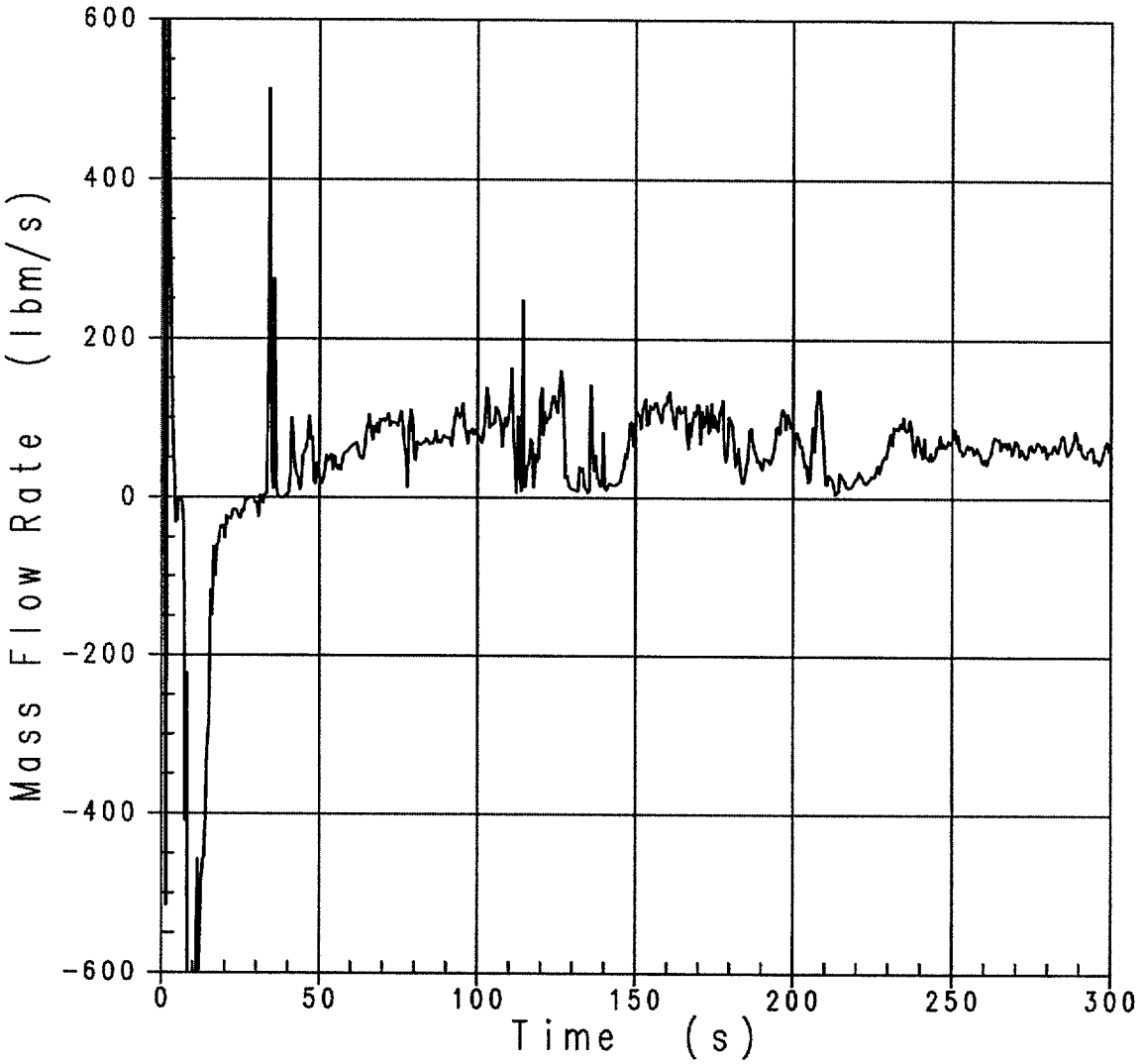
Appendix K Calculation
Accumulator Flow
Figure 14.3.2-9



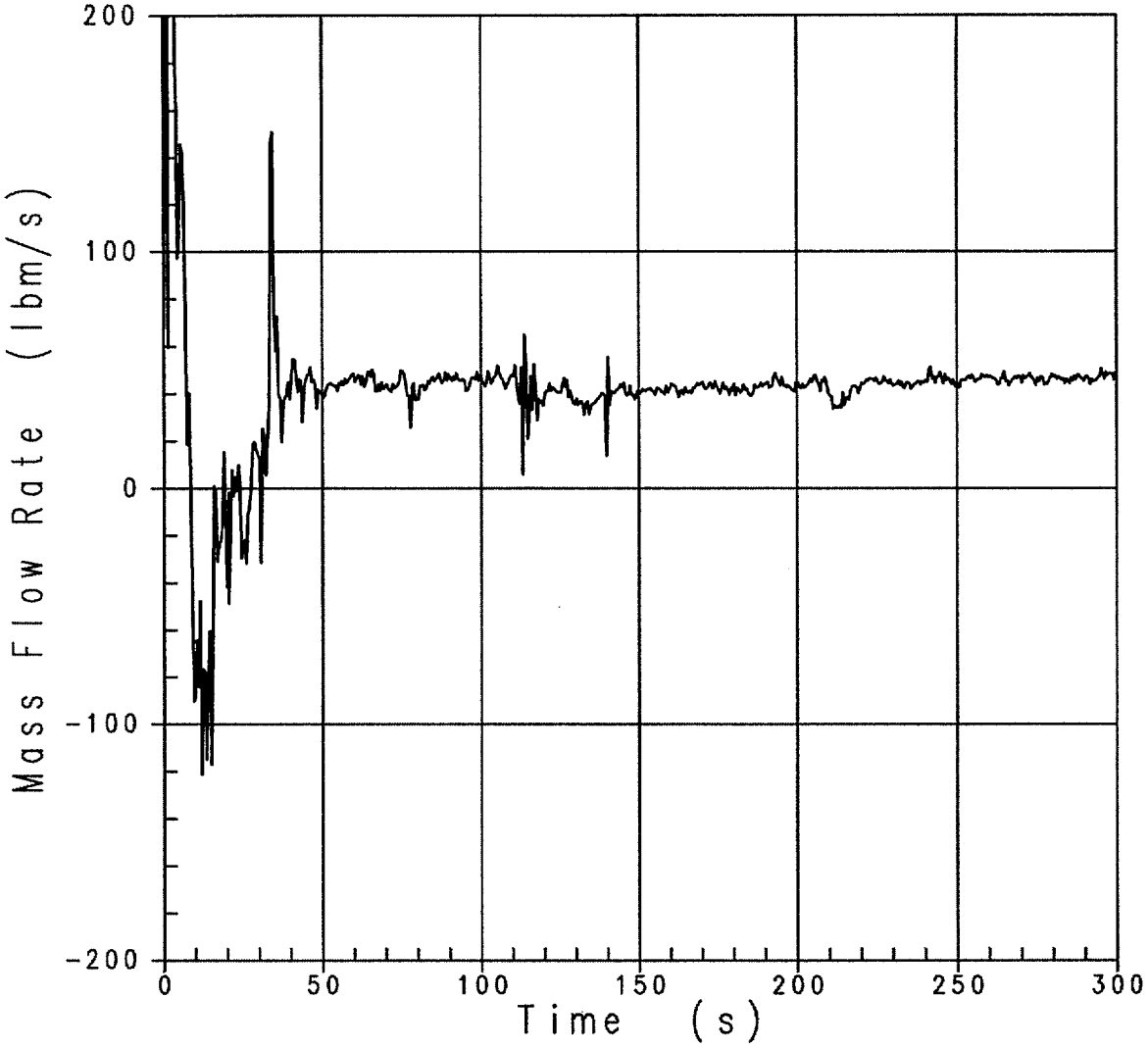
Appendix K Calculation
High Head Safety Injection Flow
Figure 14.3.2-10



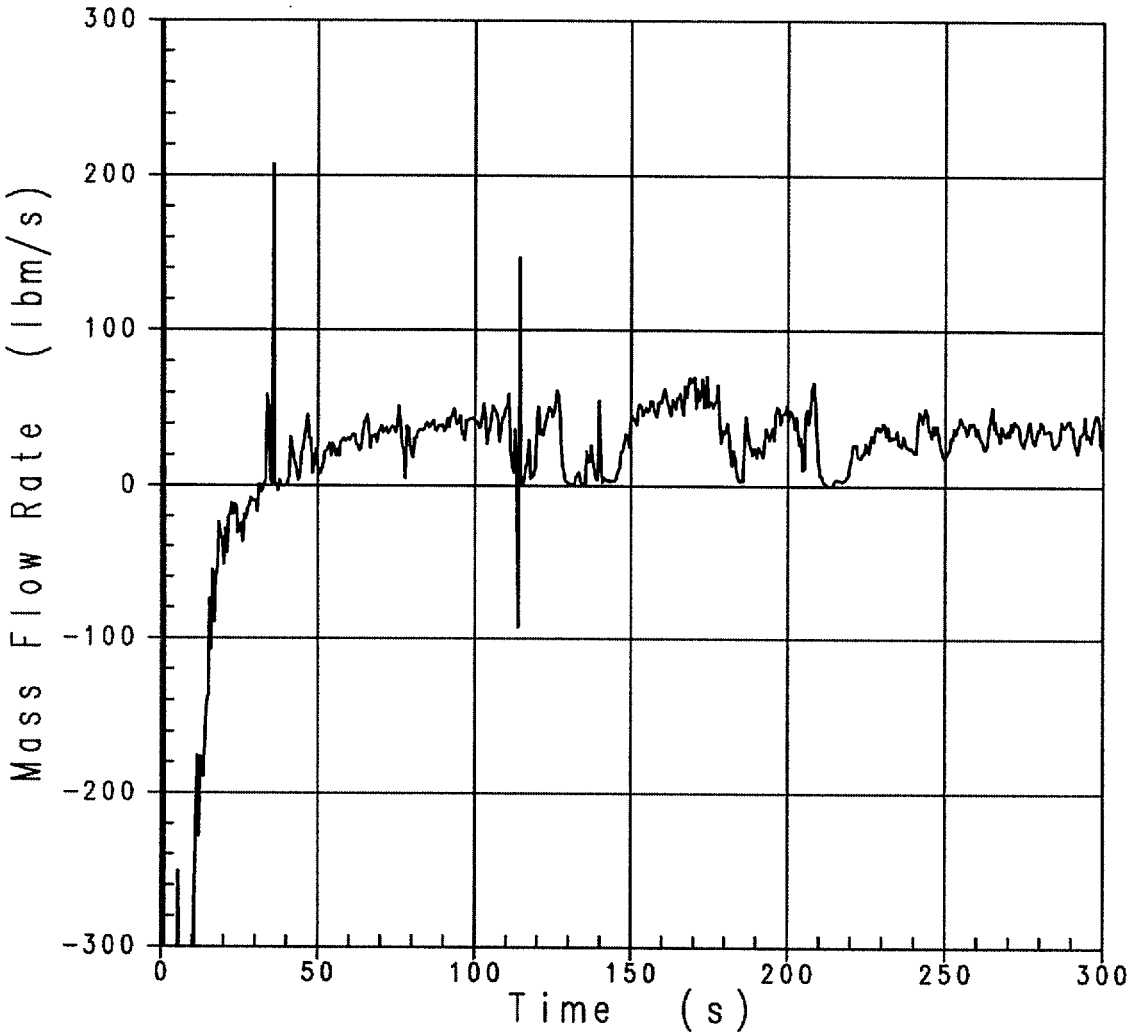
Appendix K Calculation
Low Head Safety Injection Flow
Figure 14.3.2-11



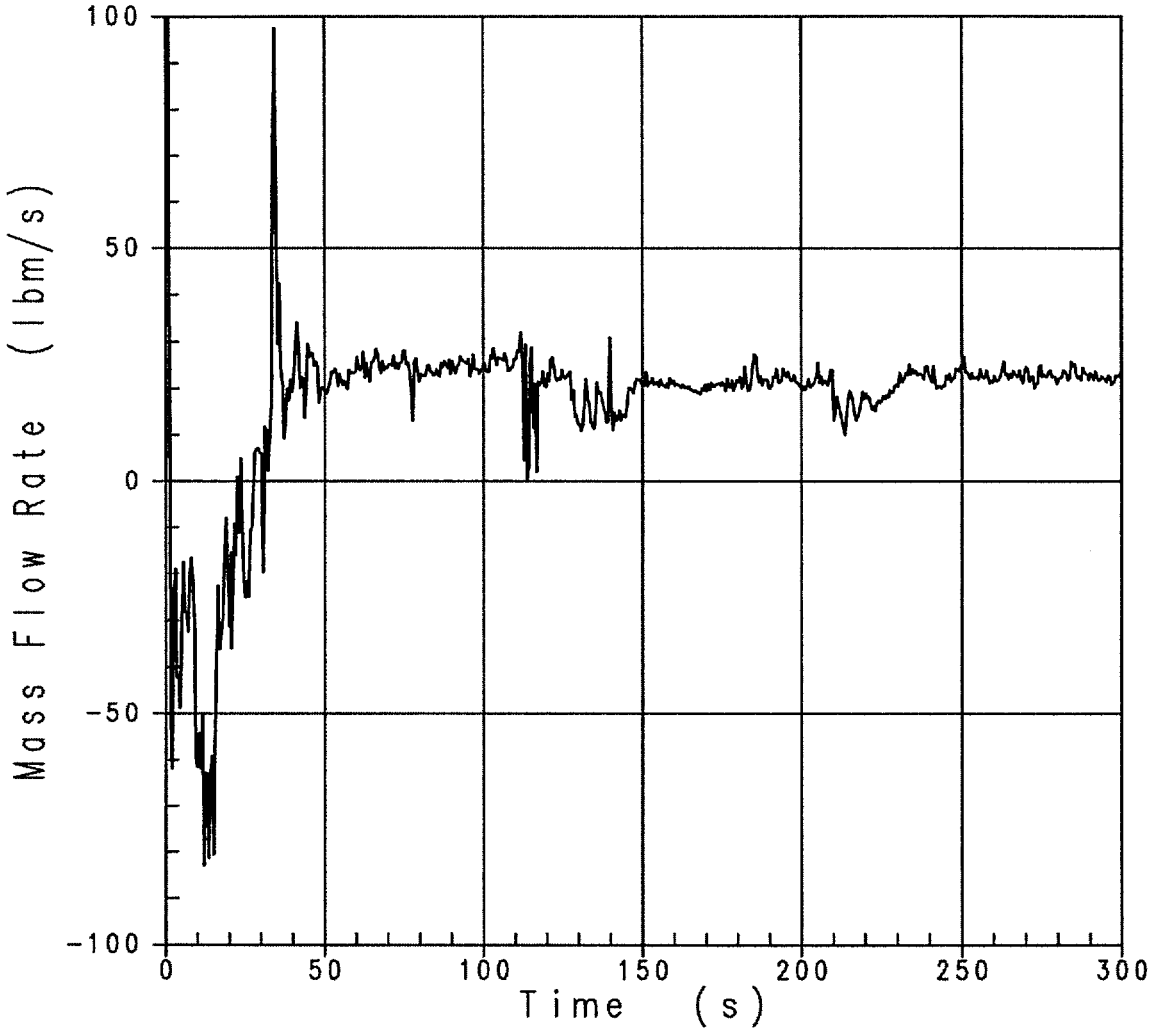
Appendix K Calculation
Liquid Flow at Top of Core Channel 10 (OH/SC)
Figure 14.3.2-12



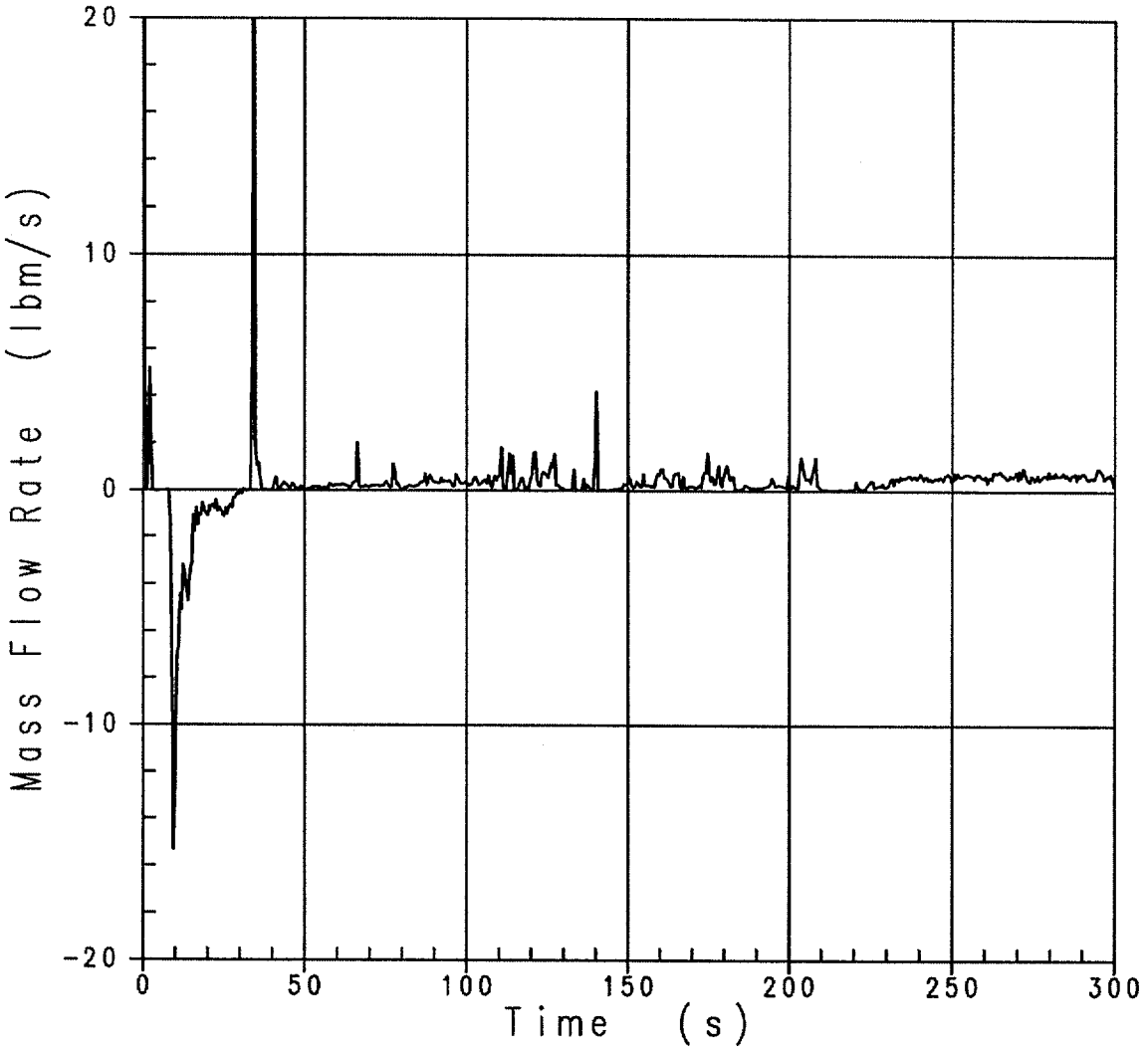
Appendix K Calculation
Vapor Flow at Top Of Core Channel 10 (OH/SC)
Figure 14.3.2-13



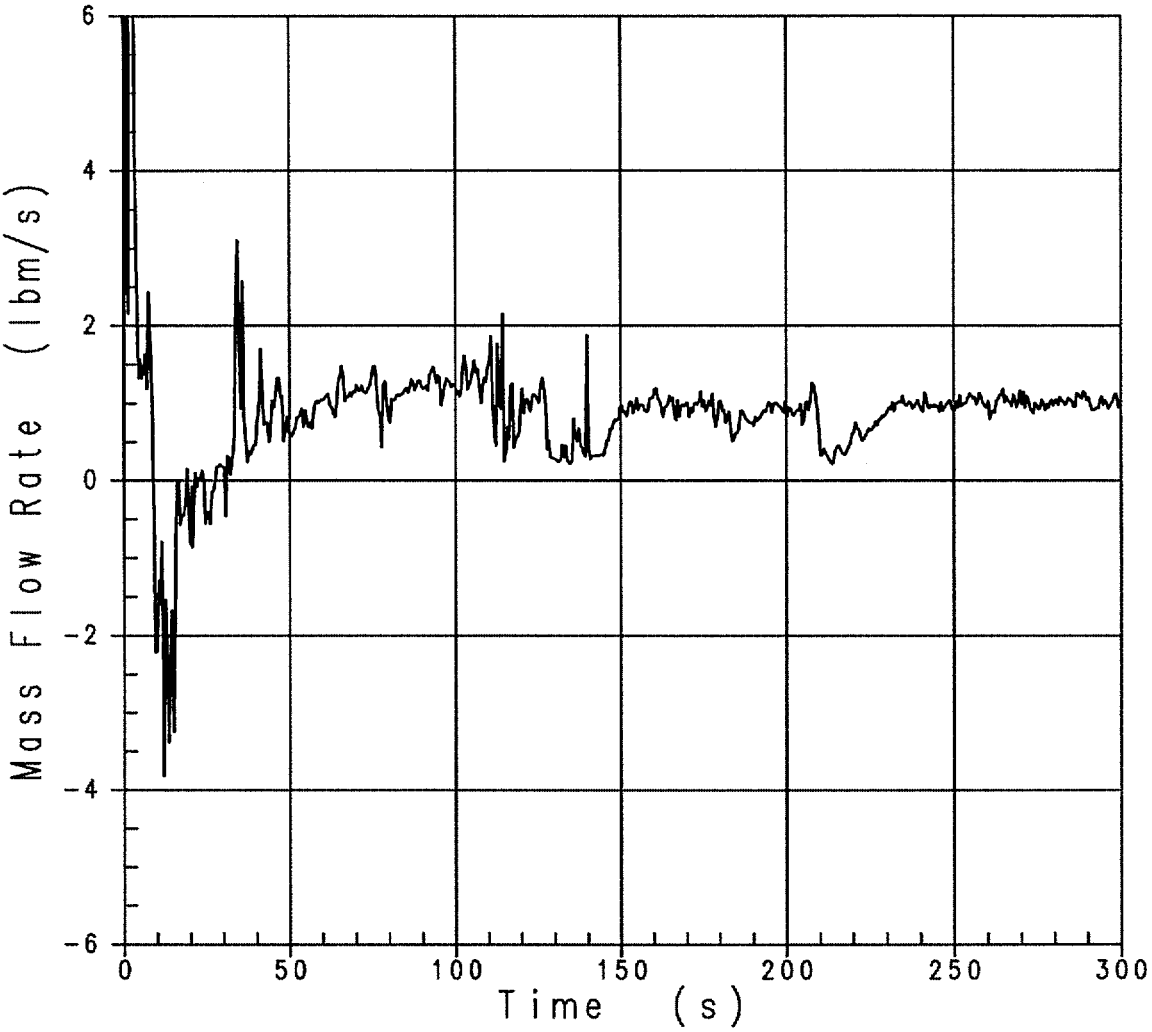
Appendix K Calculation
Liquid Flow at Top of Core Channel 11 (GT)
Figure 14.3.2-14



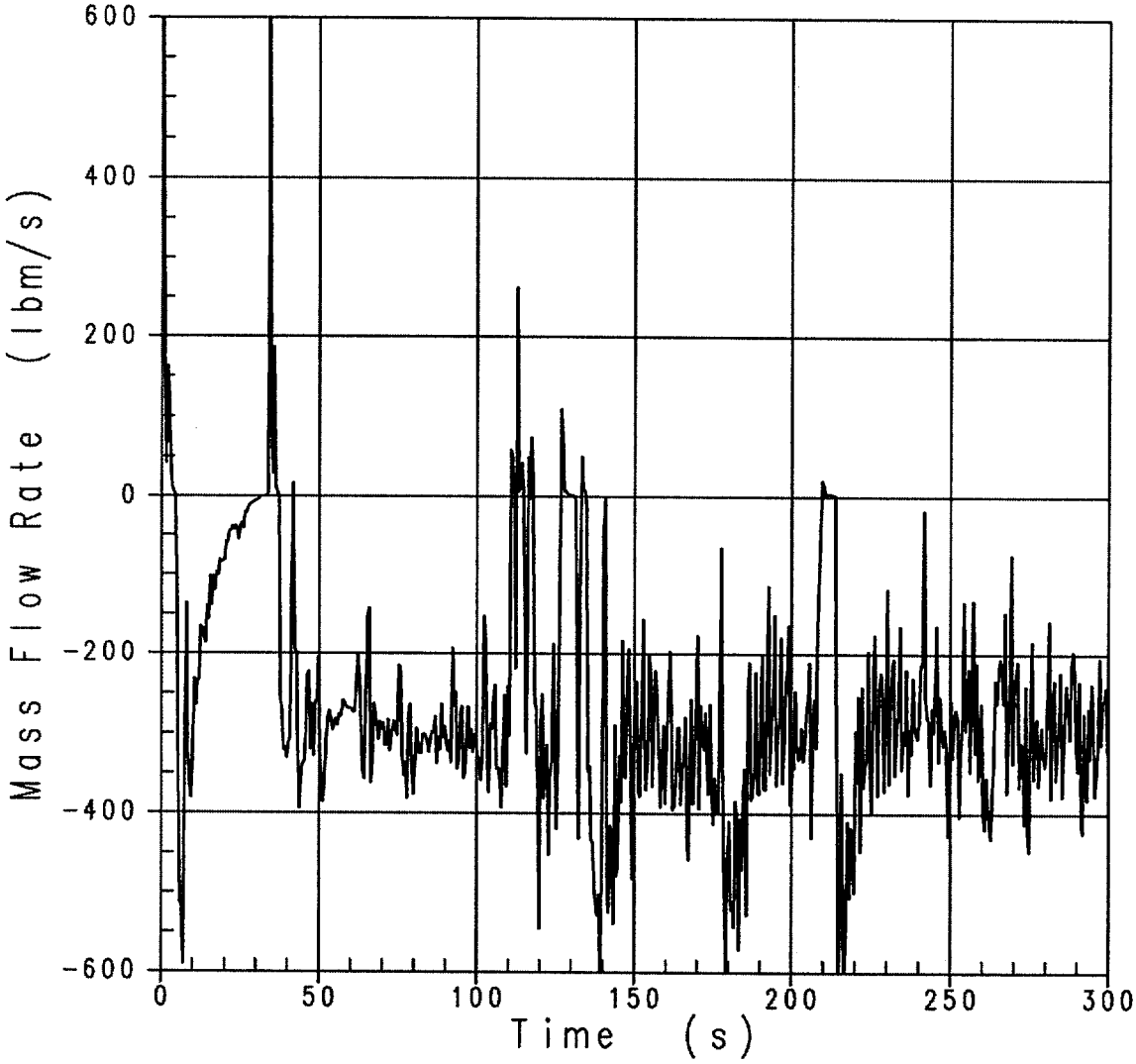
Appendix K Calculation
Vapor Flow at Top of Core Channel 11 (GT)
Figure 14.3.2-15



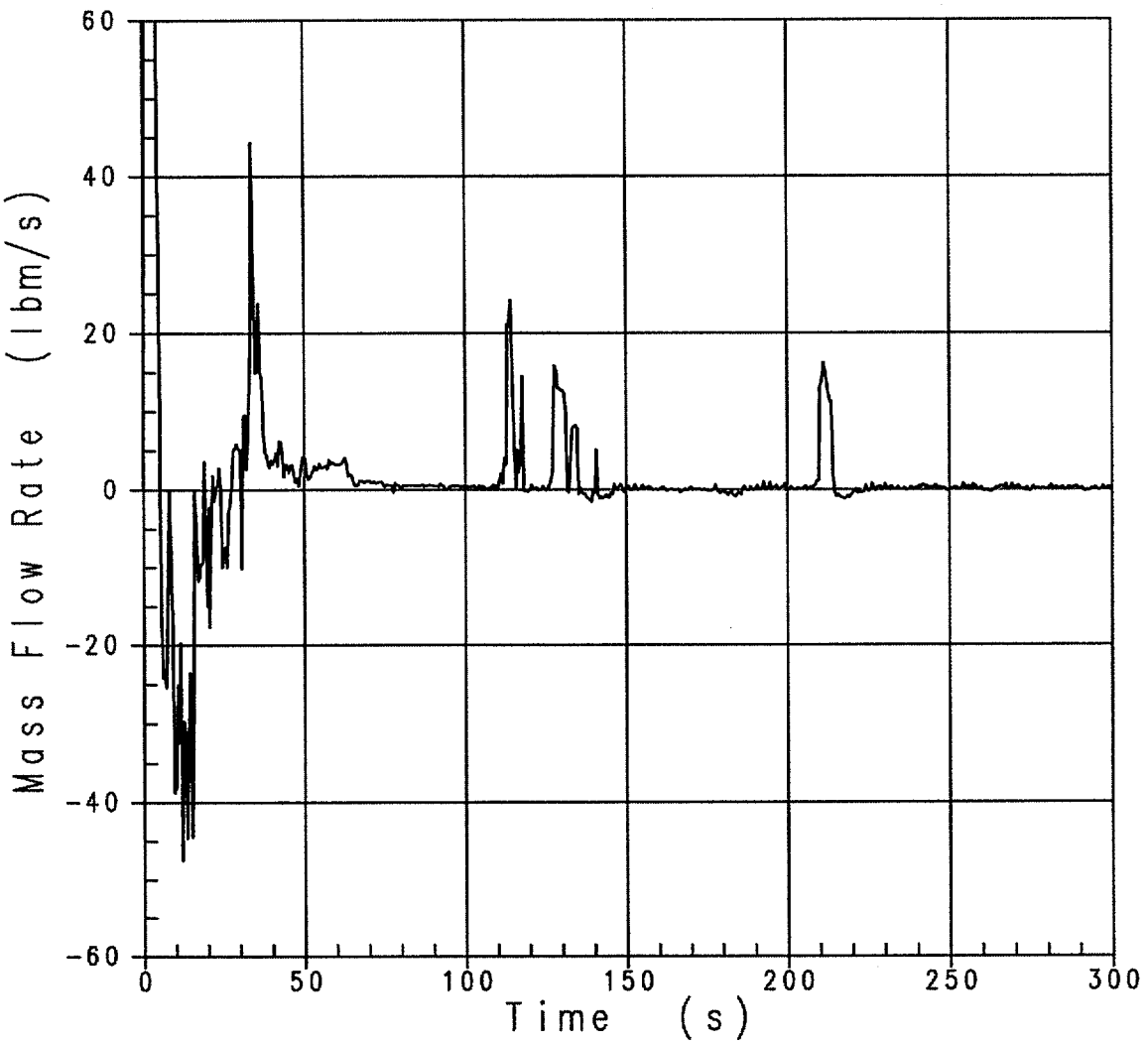
Appendix K Calculation
Liquid Flow at Top of Core Channel 12 (HA)
Figure 14.3.2-16



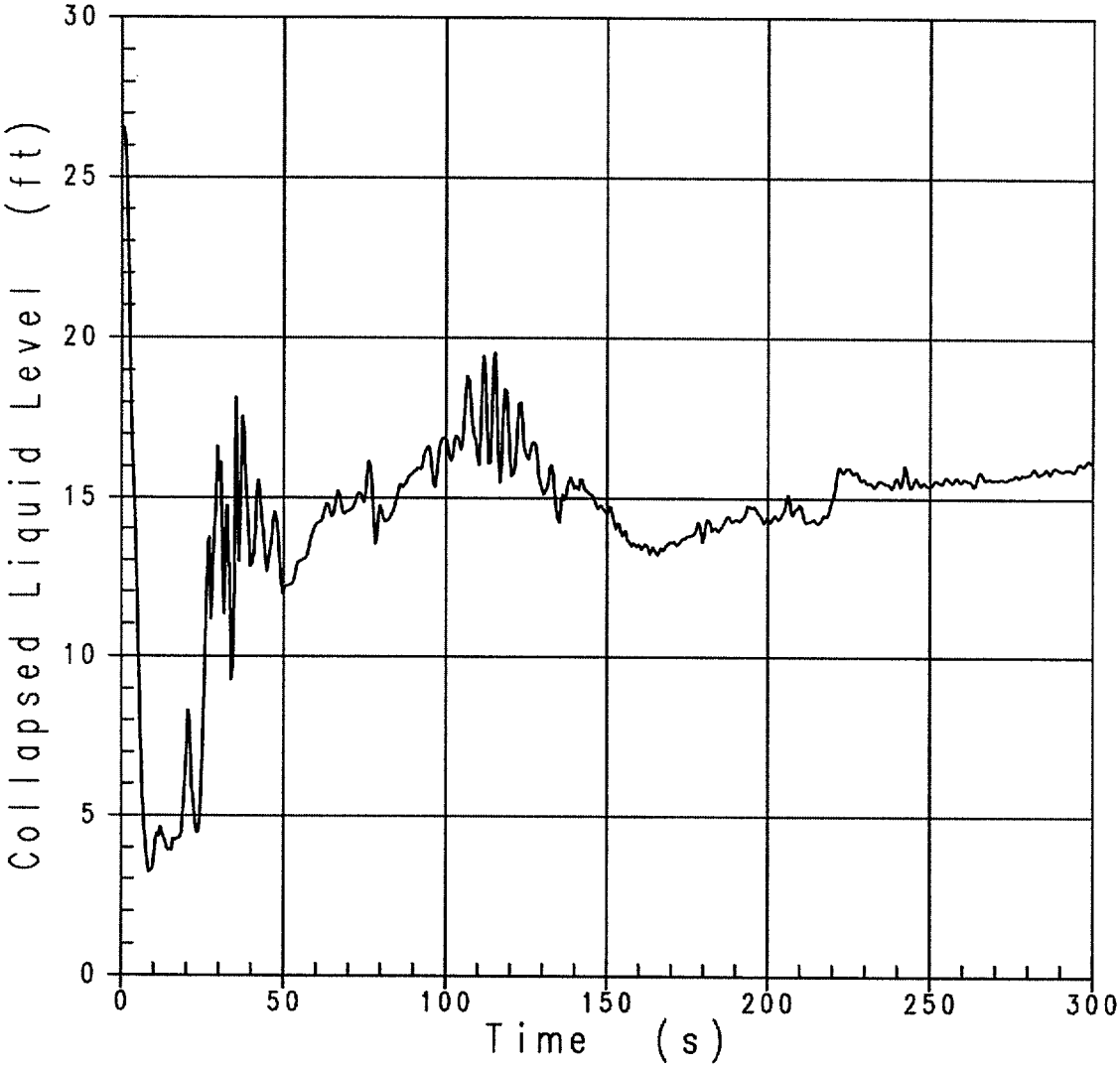
Appendix K Calculation
Vapor Flow at Top of Core Channel 12 (HA)
Figure 14.3..2-17



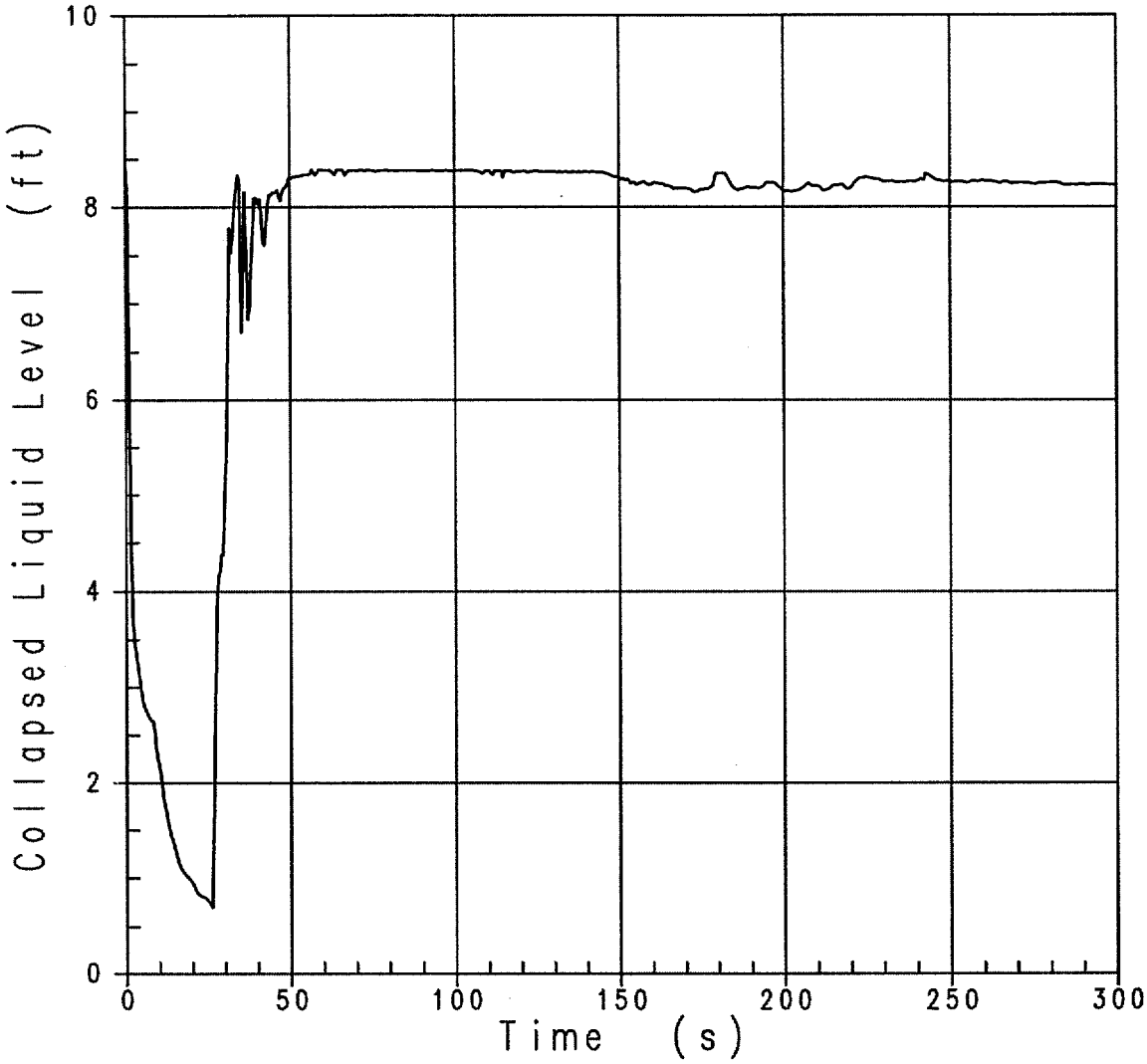
Appendix K Calculation
Liquid Flow at Top of Core Channel 13 (LP)
Figure 14.3.2-18



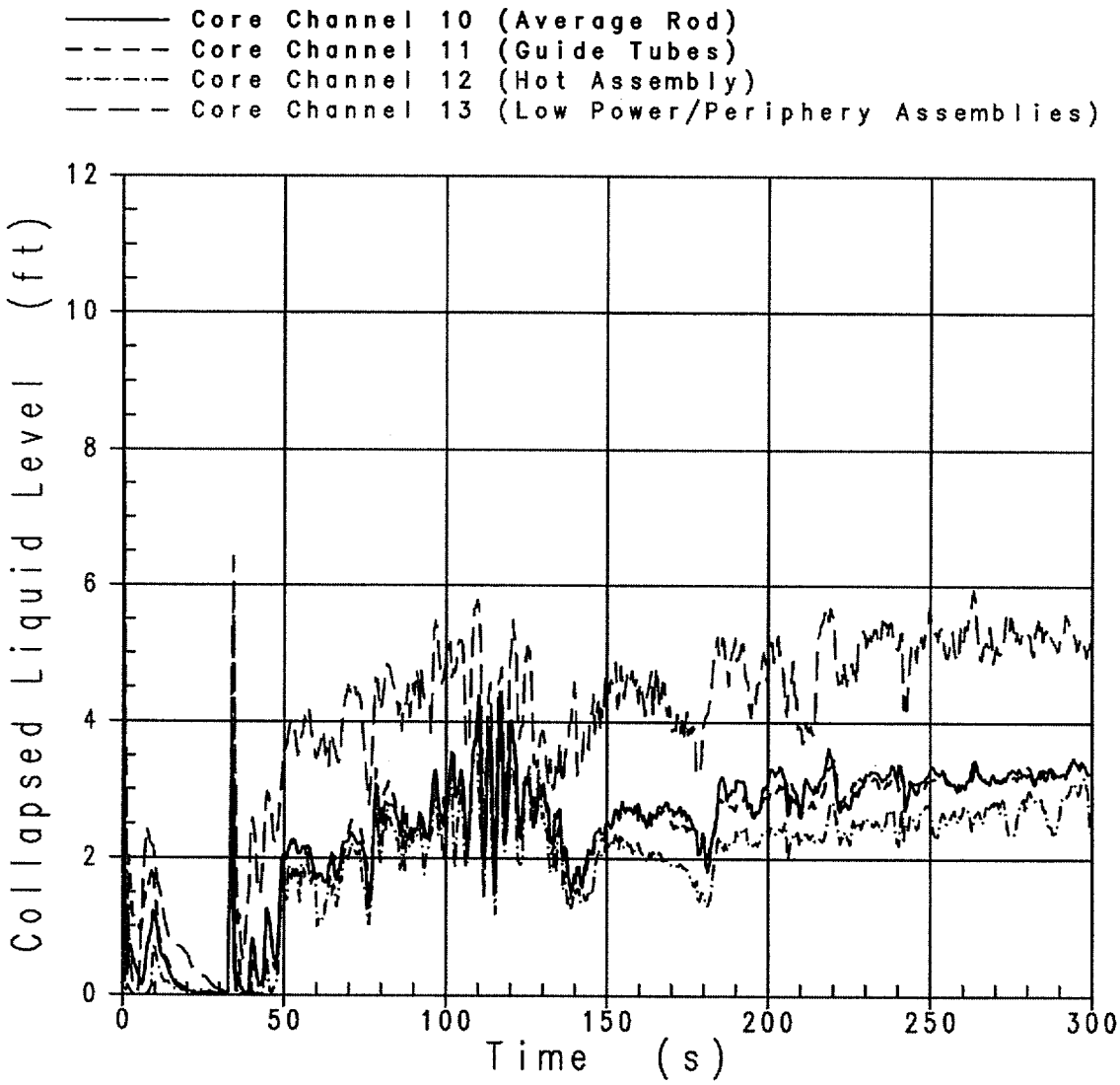
Appendix K Calculaiton
Vaport Flow at Top of Core Channel 13 (LP)
Figure 14.3.21-19



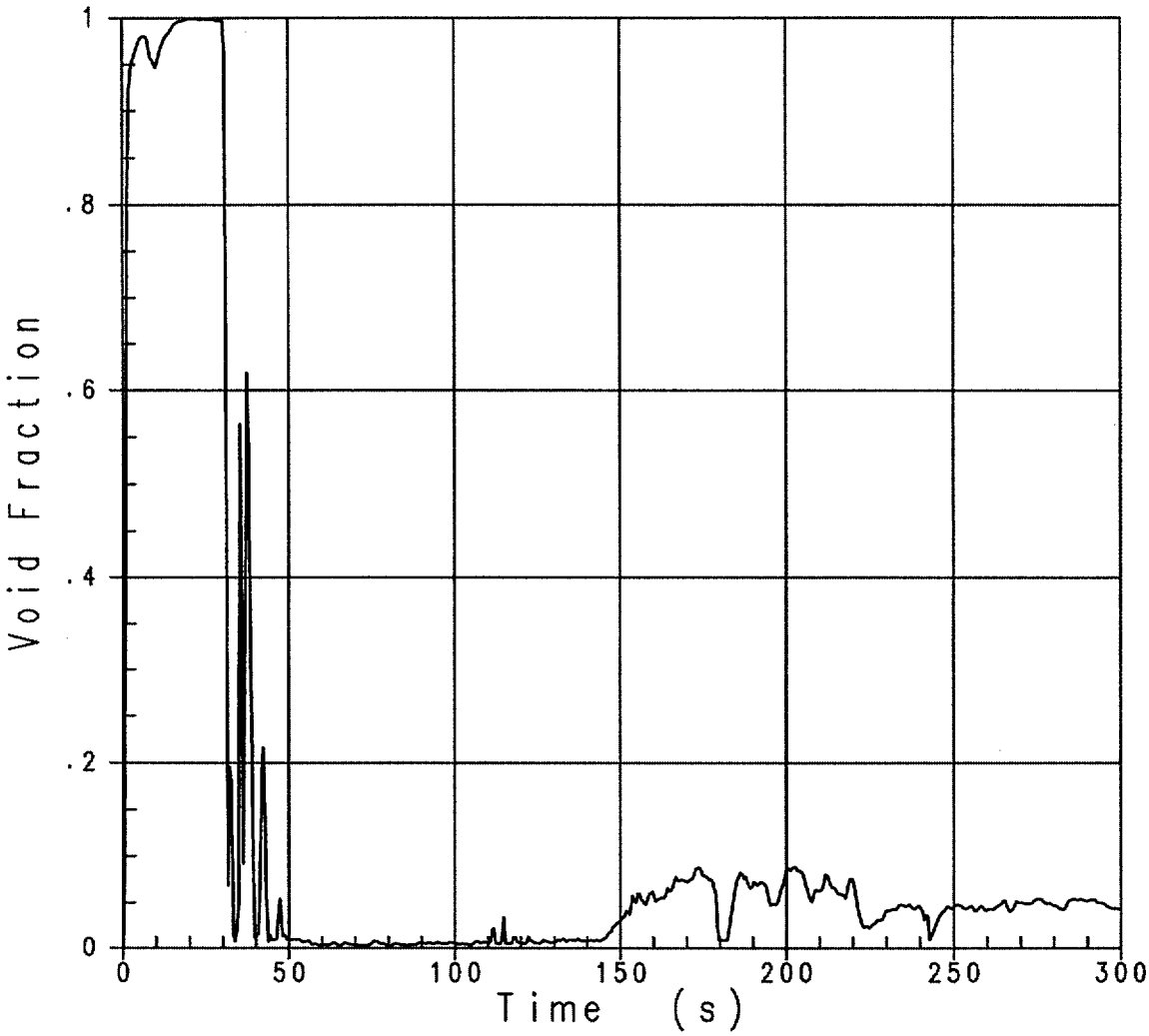
Appendix K Calculation
Downcomer Liquid Level
Figure 14.3.2-20



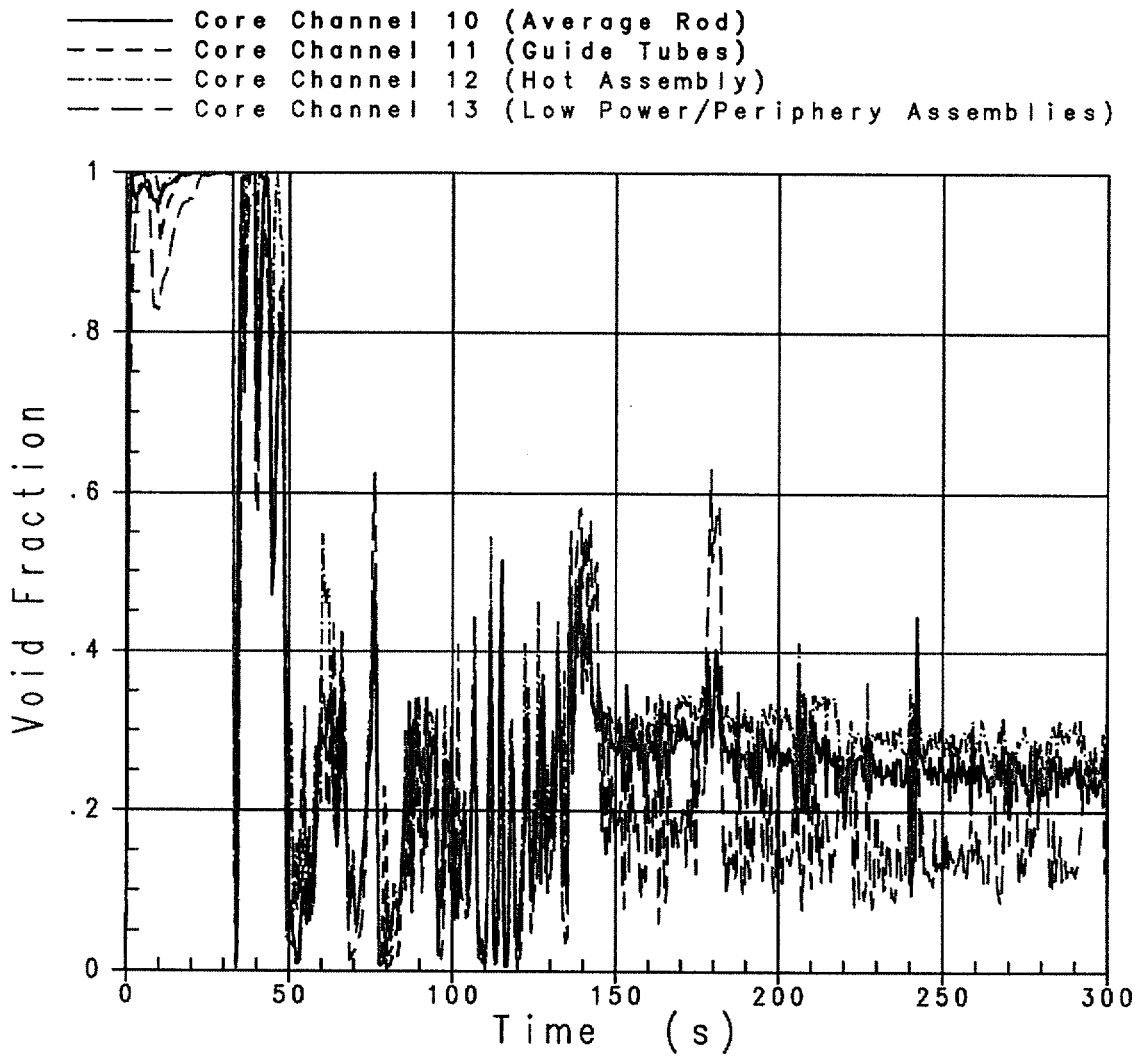
Appendix K Calculation
Lower Plenum Liquid Level
Figure 14.3.2-21



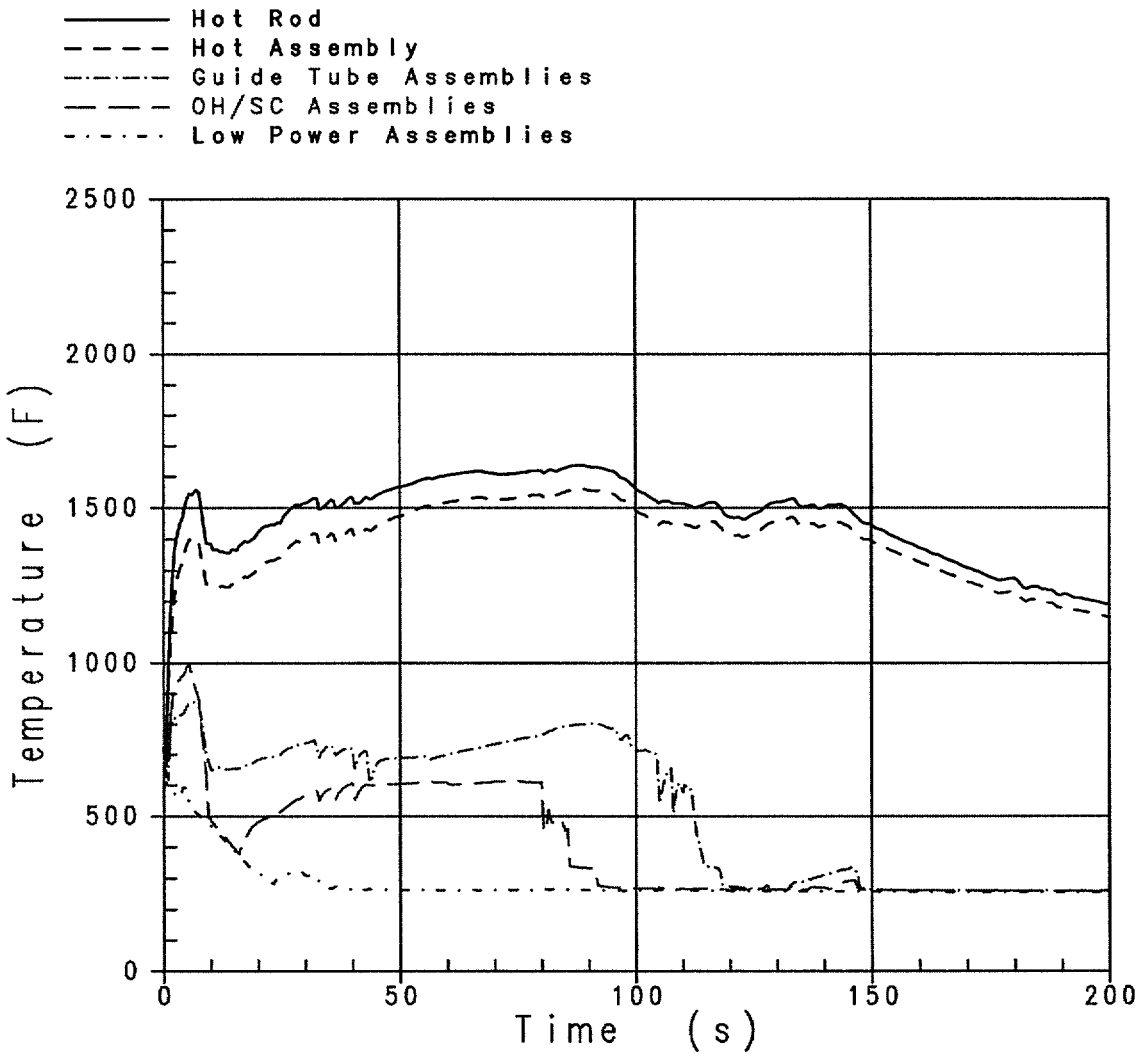
Appendix K Calculation
Core Liquid Level
Figure 14.3.2-22



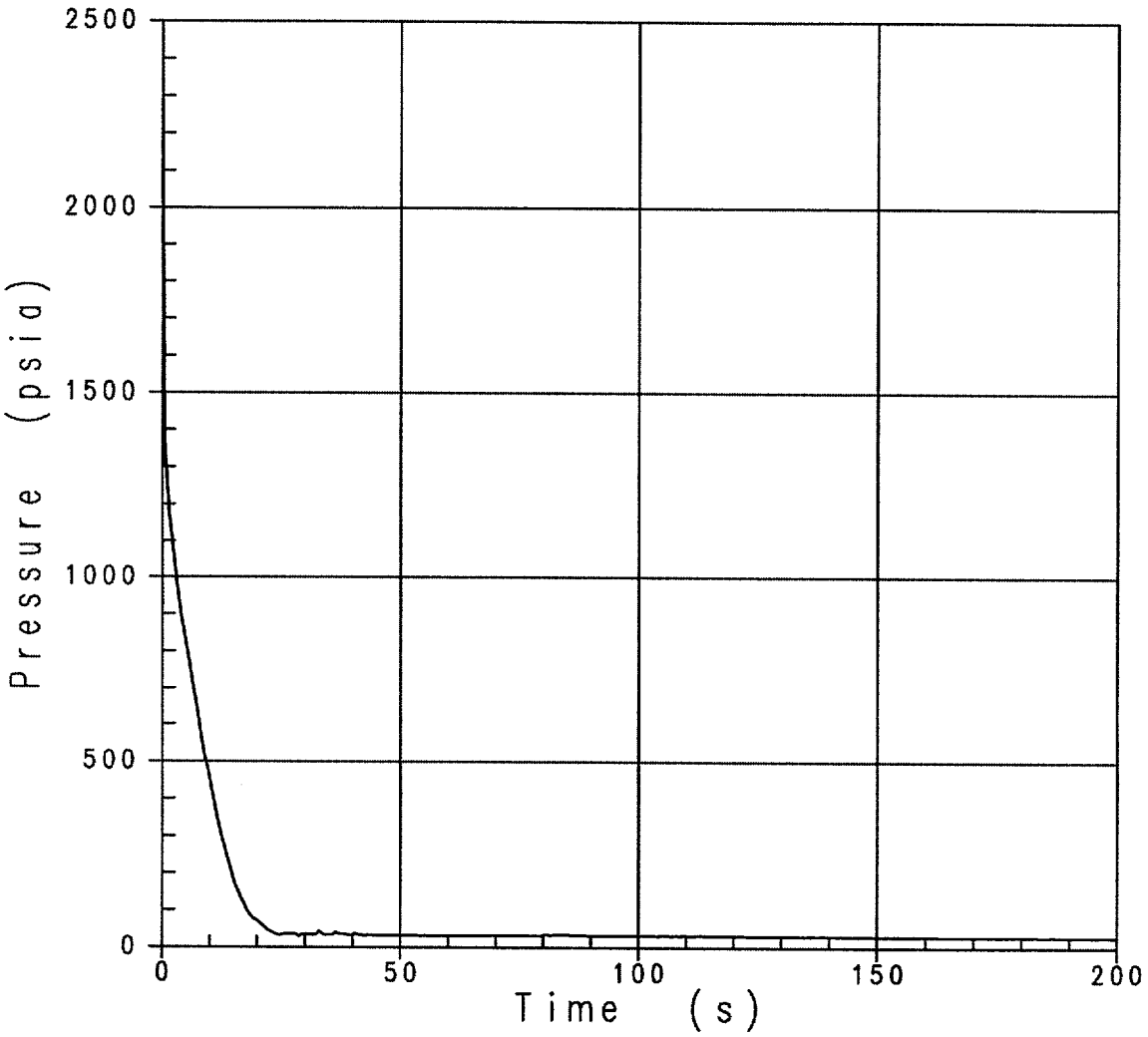
Appendix K Calculation
Void Fraction in Lower Plenum
Figure 14.3.2-23



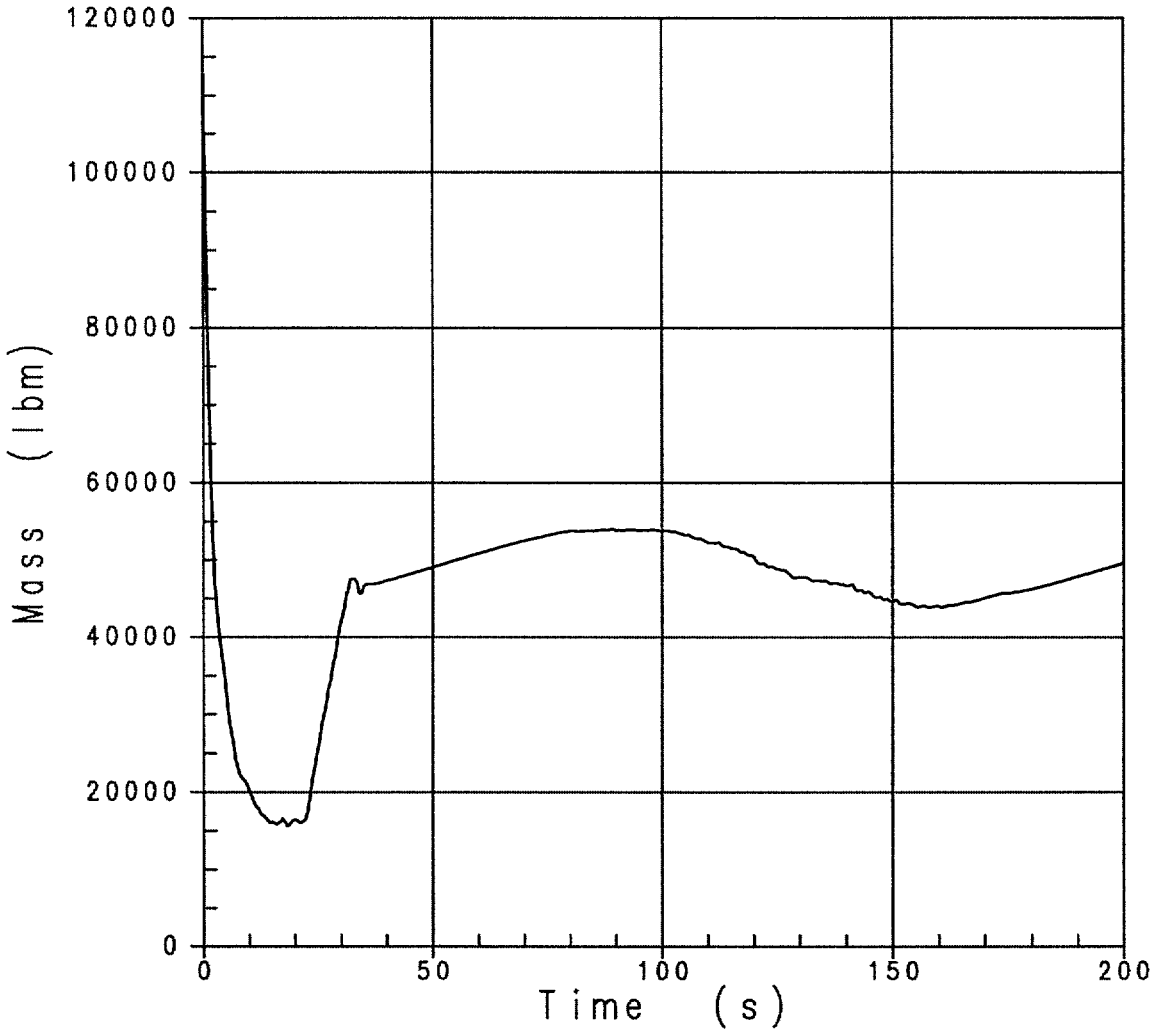
Appendix K Calculation
Void Fraction at Bottom of Core
Figure 14.3.2-24



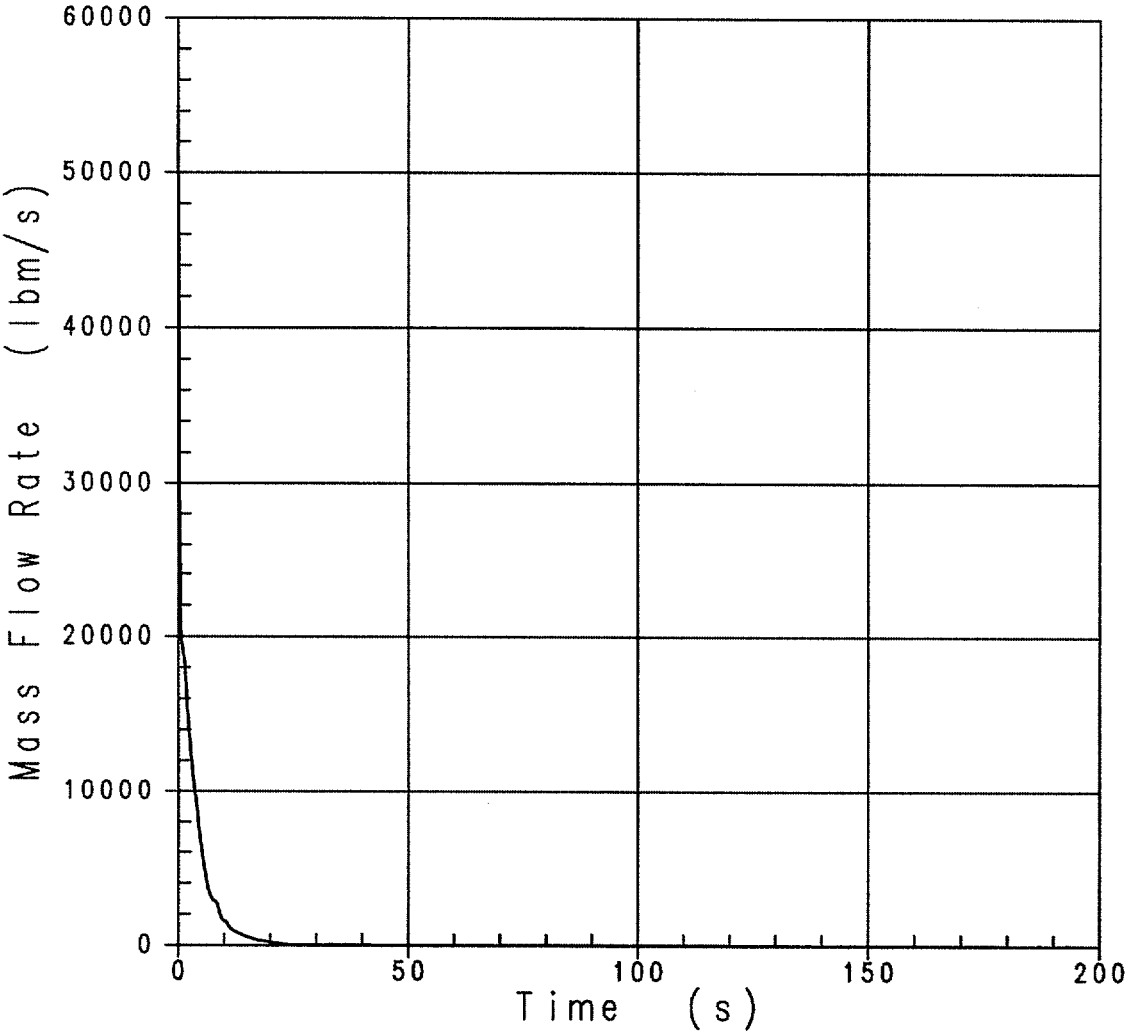
Superbounded Calculation
Peak Cladding Temperature
Figure 14.3.2-25



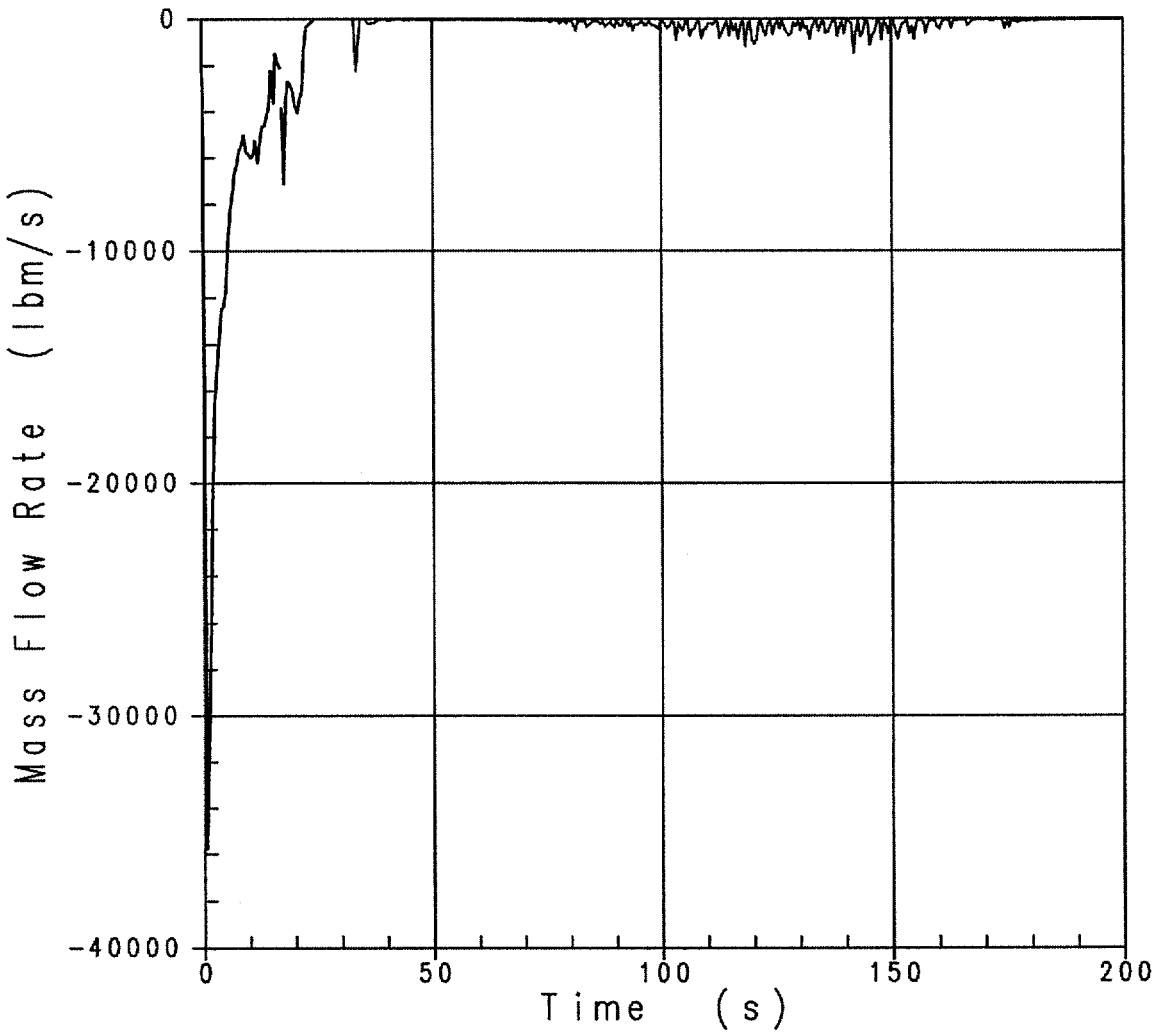
Superbounded Calculation
Core Pressure
Figure 14.3.2-26



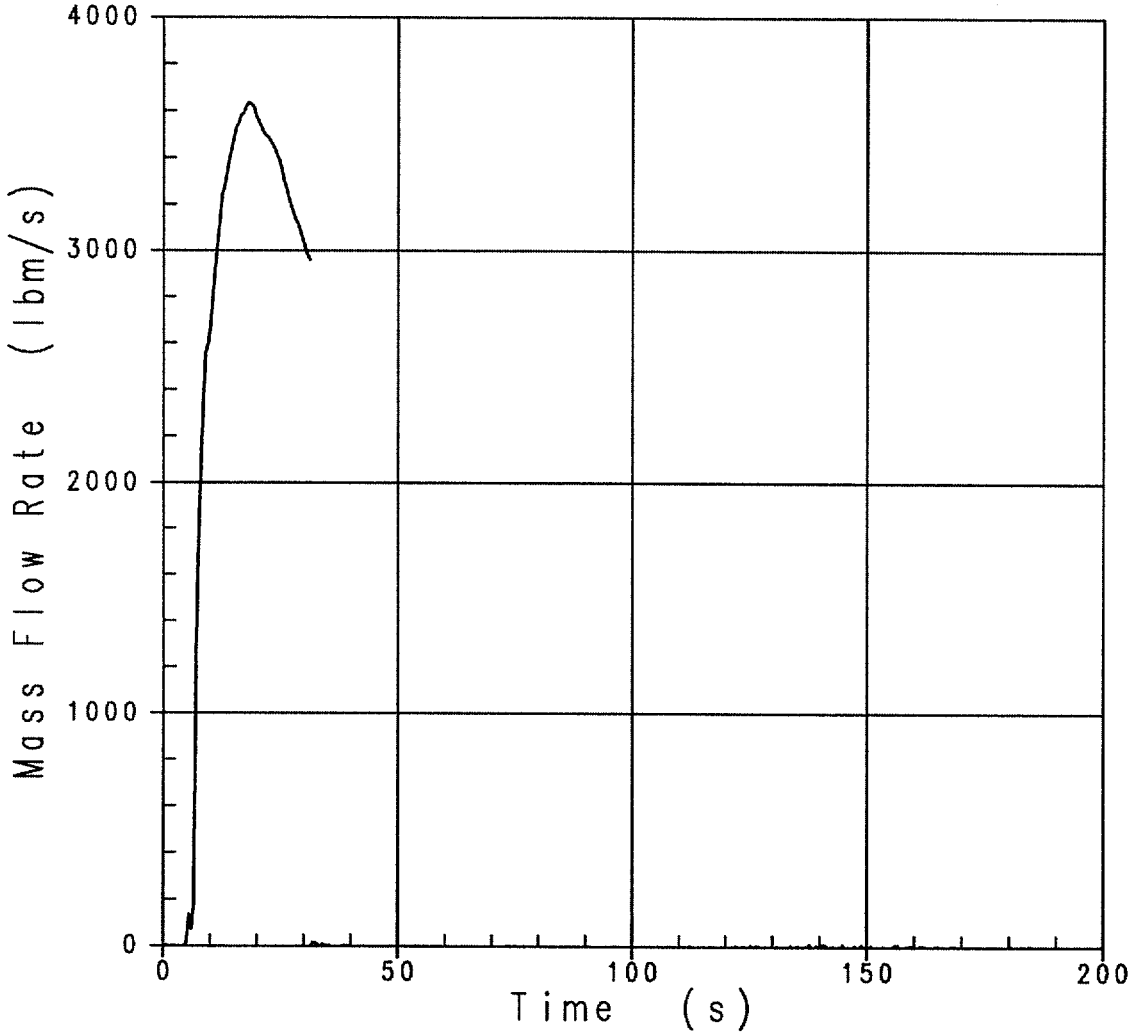
Superbounded Calculation
Vessel Inventory
Figure 14.3.2-27



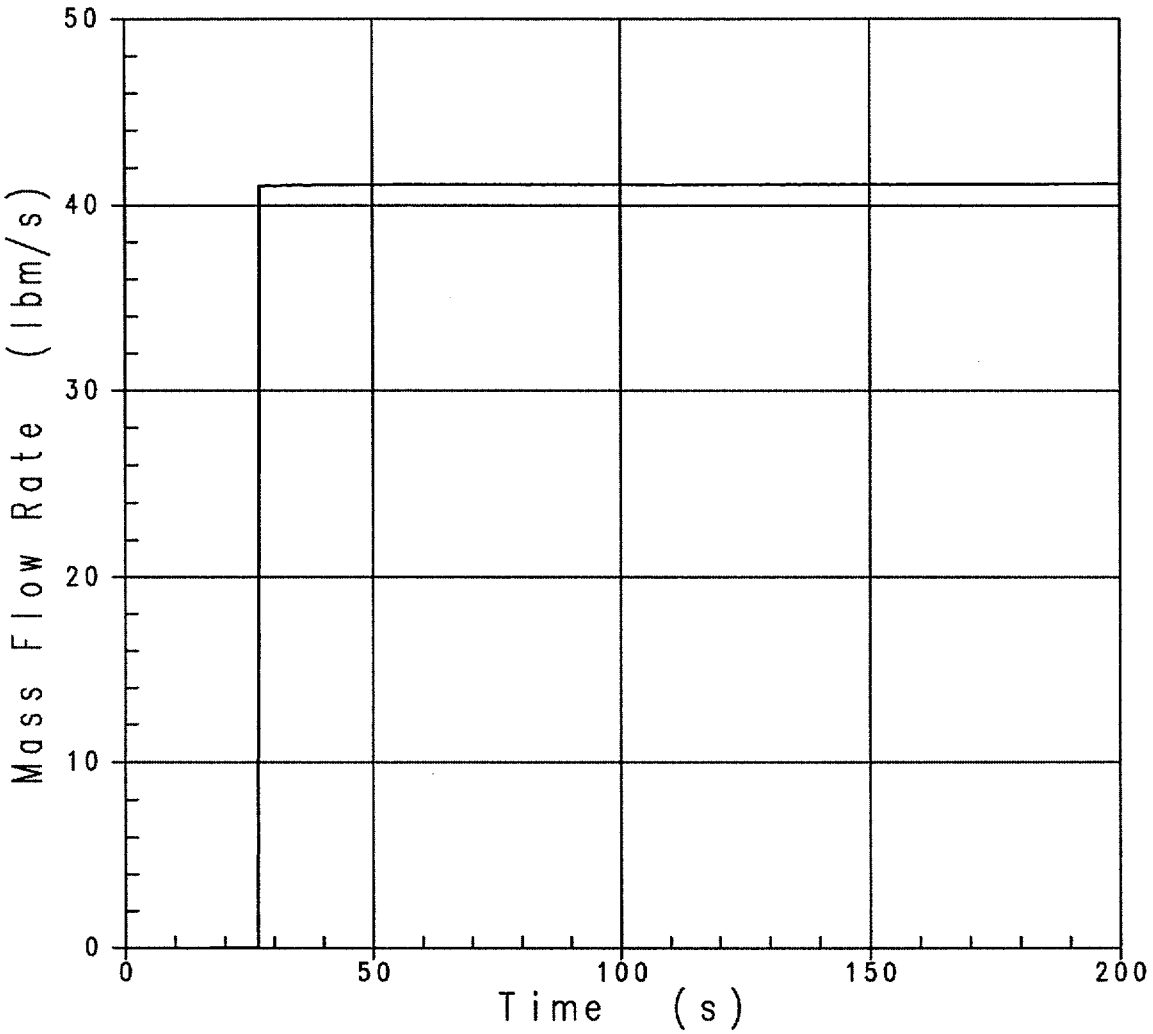
Superbounded Calculation
Loop Side Break Flow
Figure 14.3.2-28



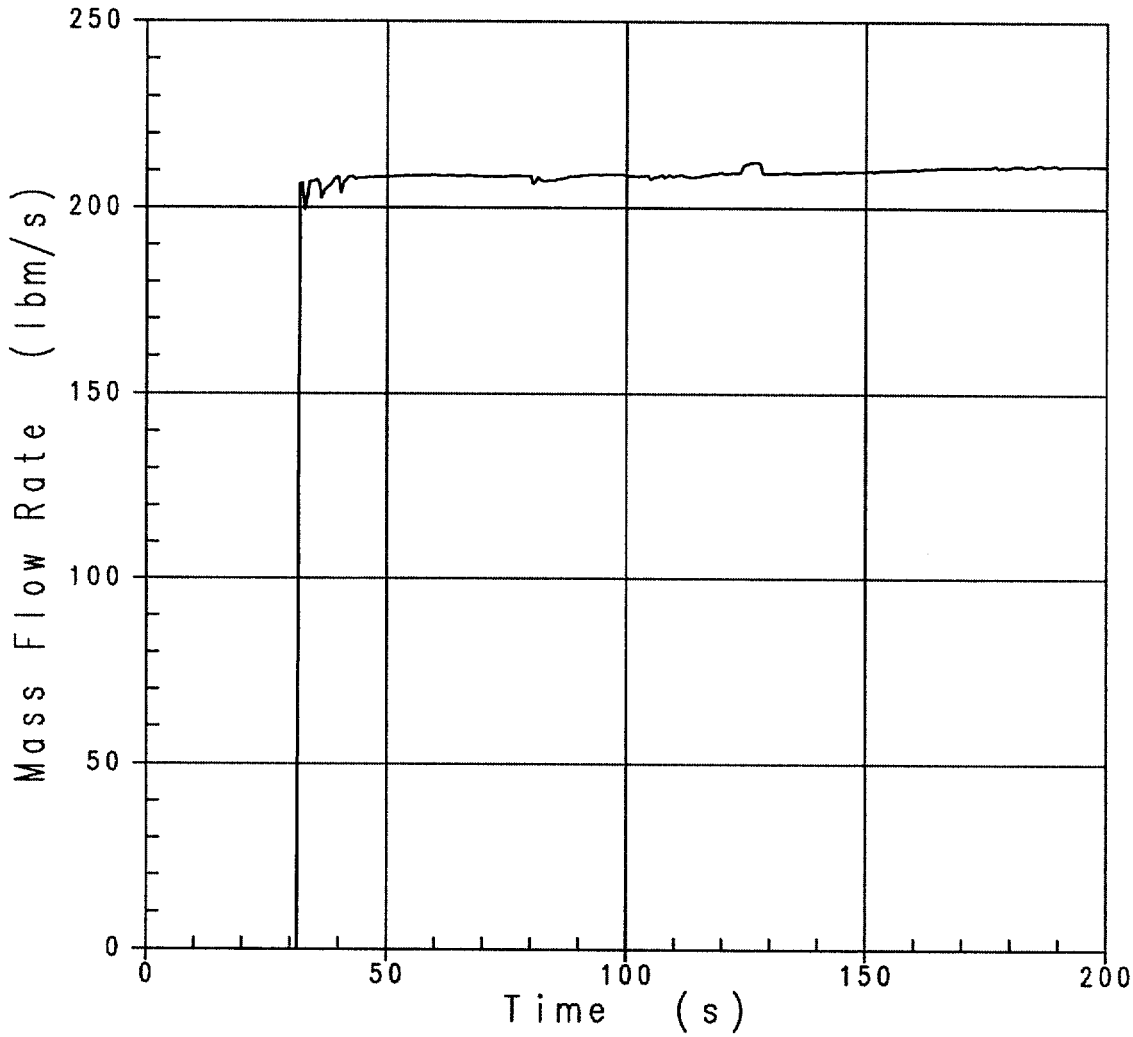
Superbounded Calculation
Vessel Side Break Flow
Figure 14.3.2-29



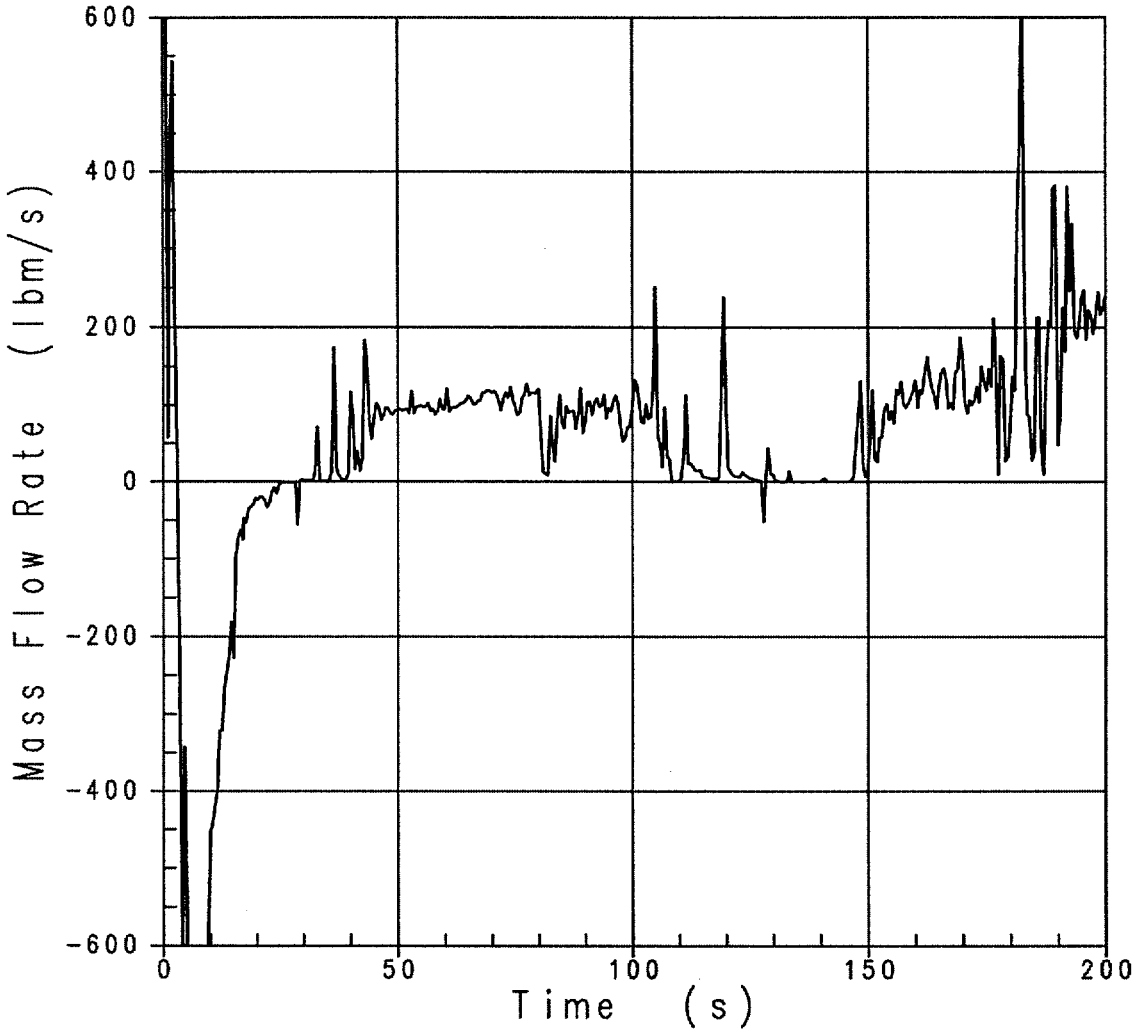
Superbounded Calculation
Accumulator Flow
Figure 14.3.2-30



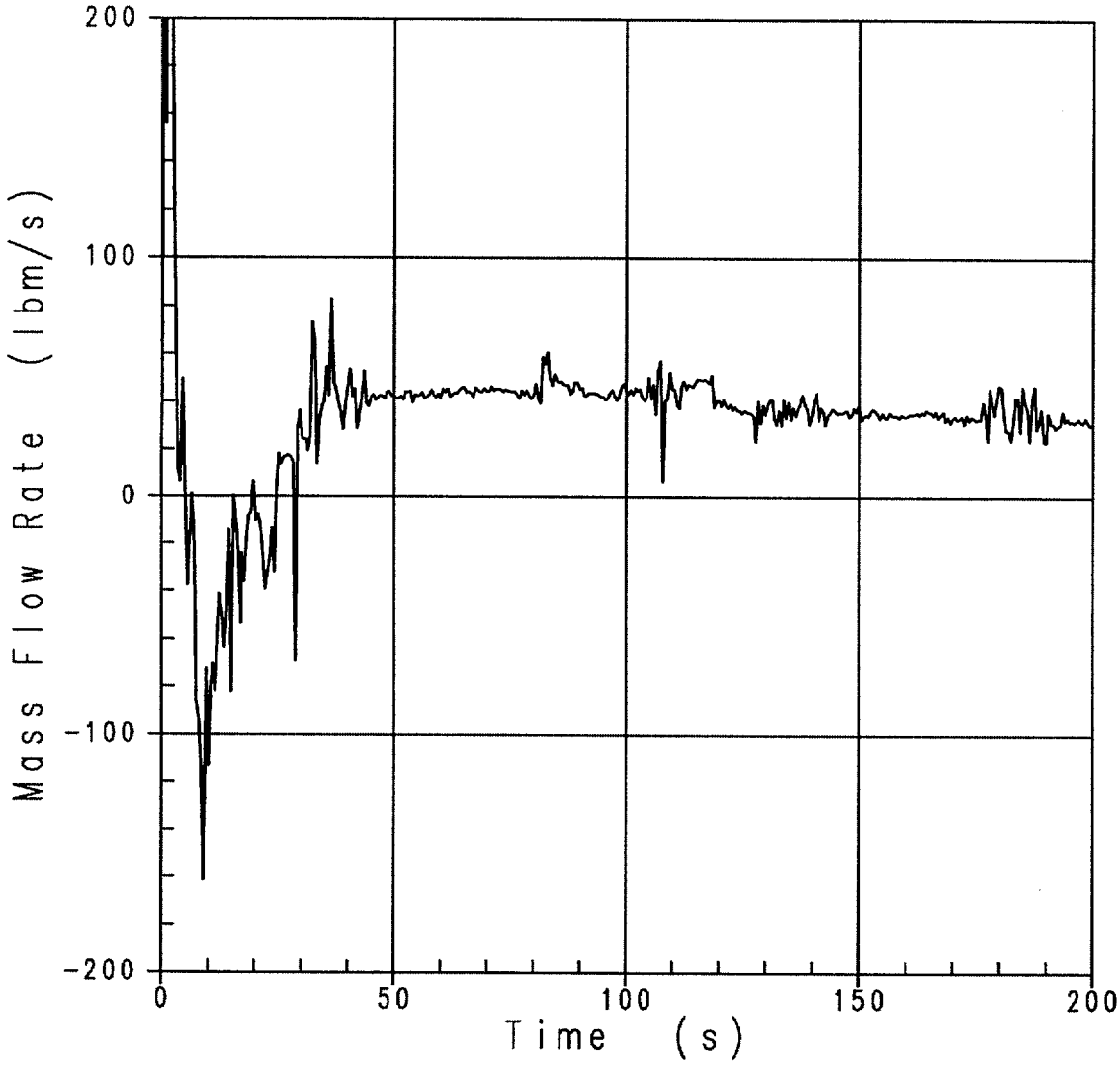
Superbounded Calculation
High Head Safety Injection Flow
Figure 14.3.2-31



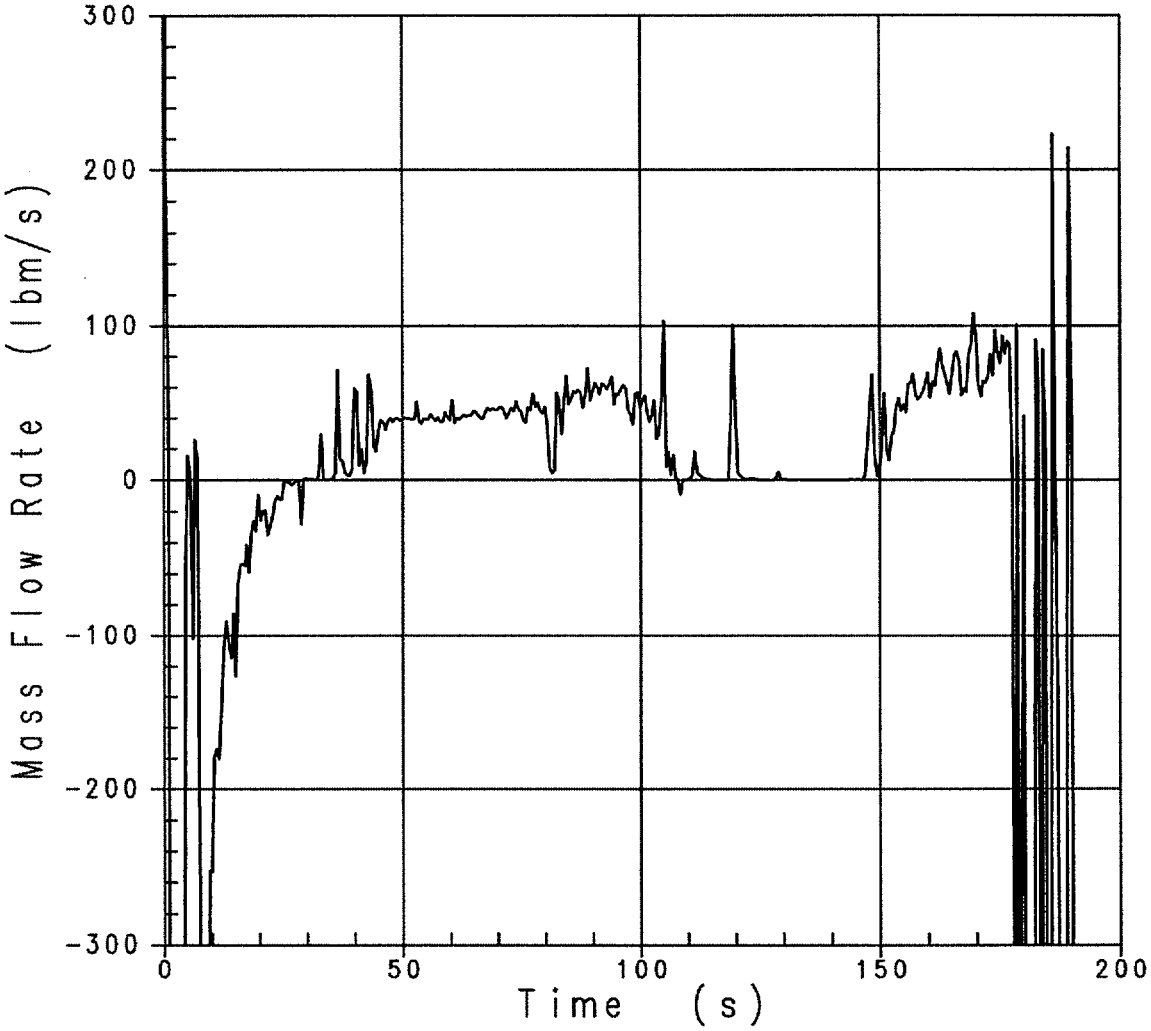
Superbounded Calculation
Low Head Safety Injection Flow
Figure 14.3.2.-32



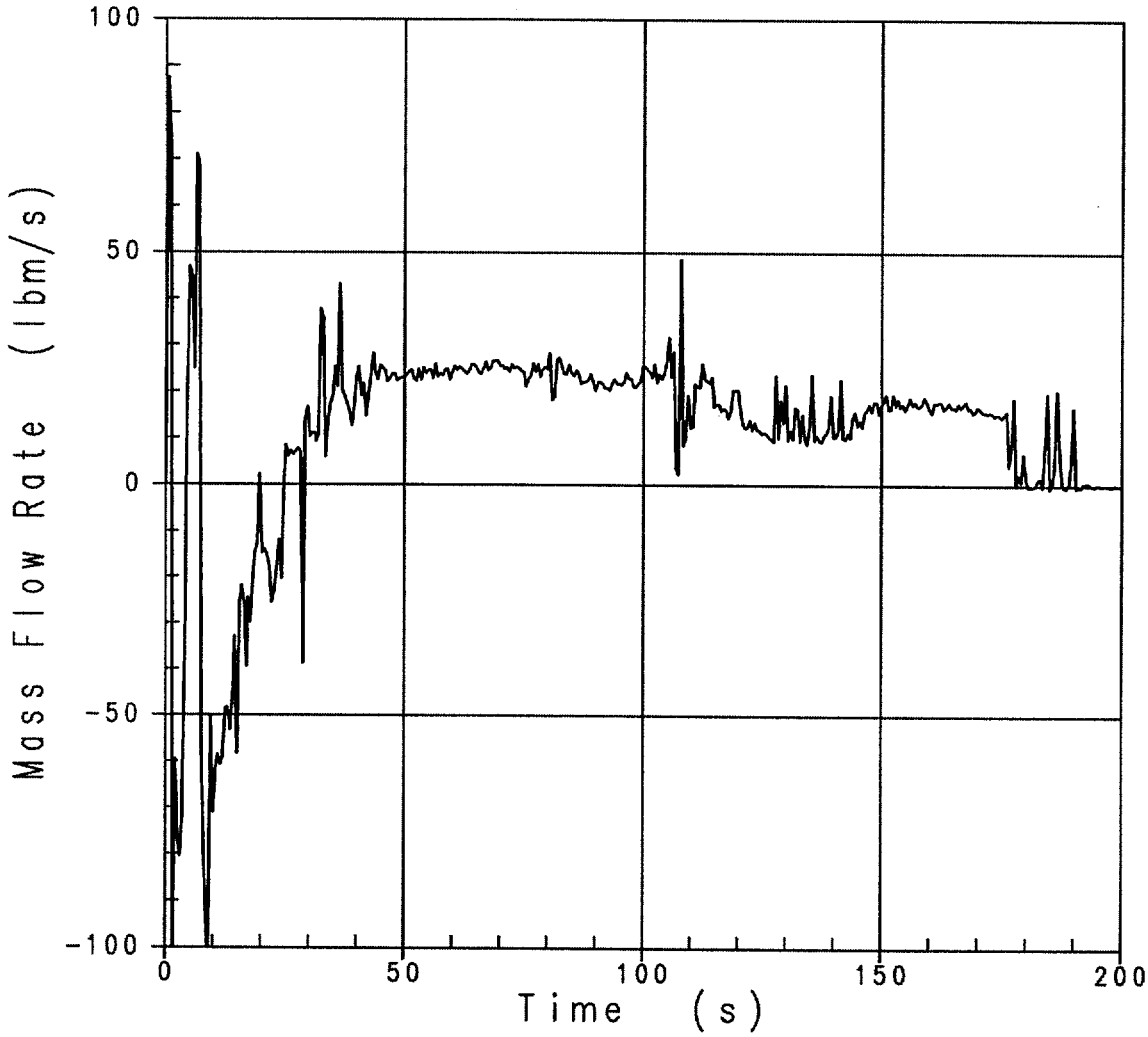
Superbounded Calculation
Liquid Flow at Top of Core Channel 10 (OH/SC)
Figure 14.3.2-33



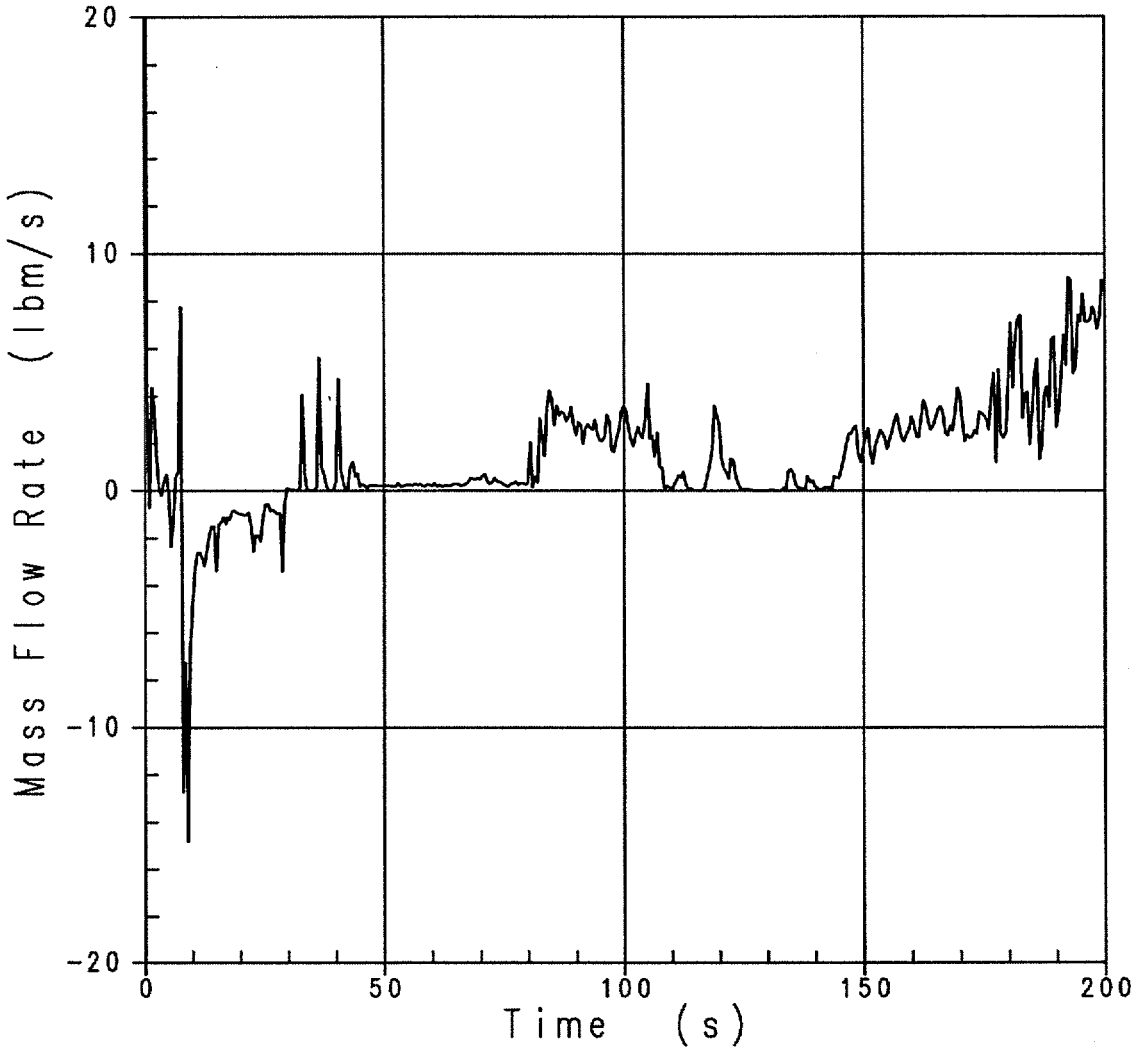
Superbounded Calculation
Vapor Flow at Top of Core Channel 10 (OH/SC)
Figure 14.3.2-34



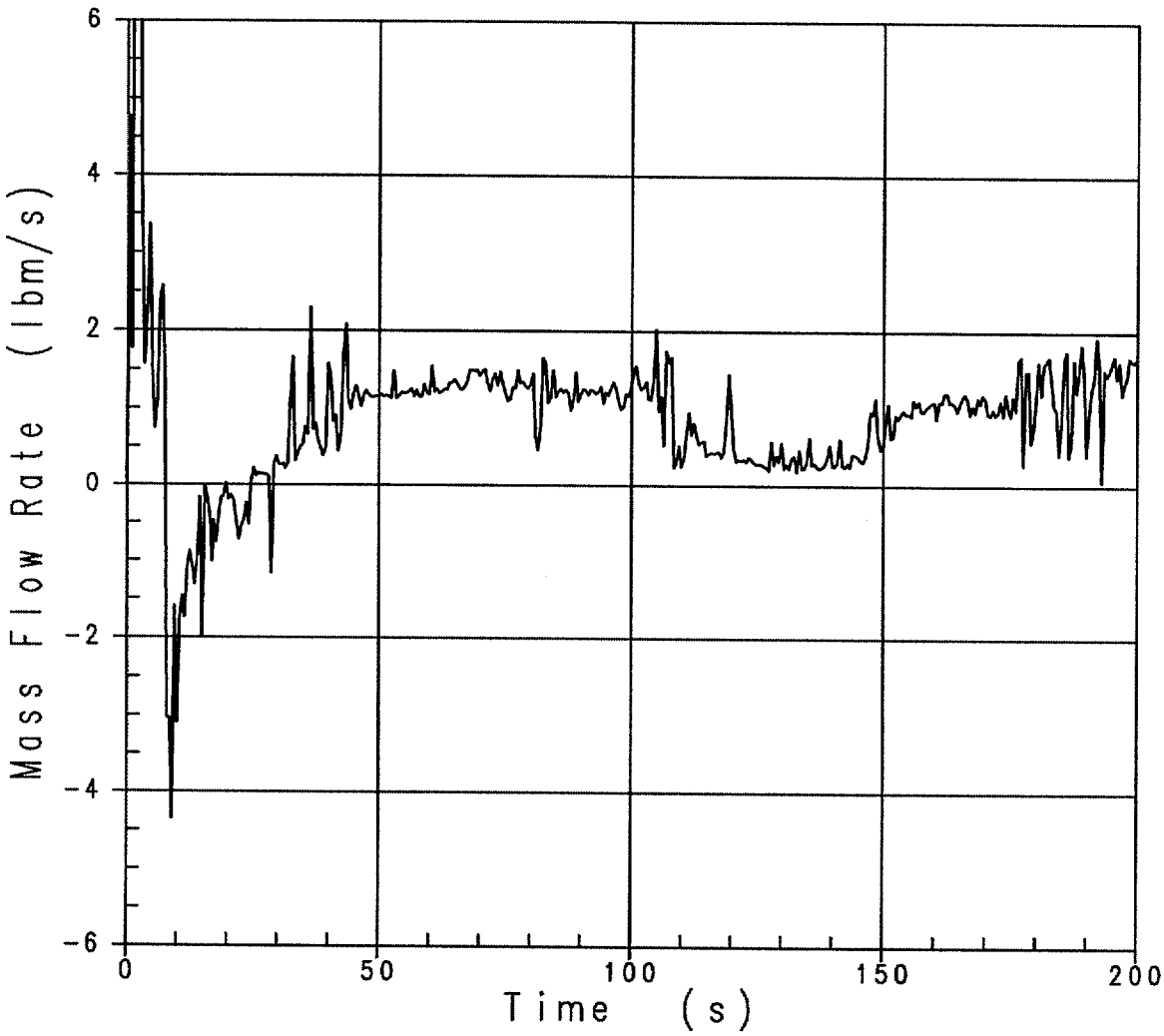
Superbounded Calculation
Liquid Flow at Top of Core Channel 11 (GT)
Figure 14.3.2-35



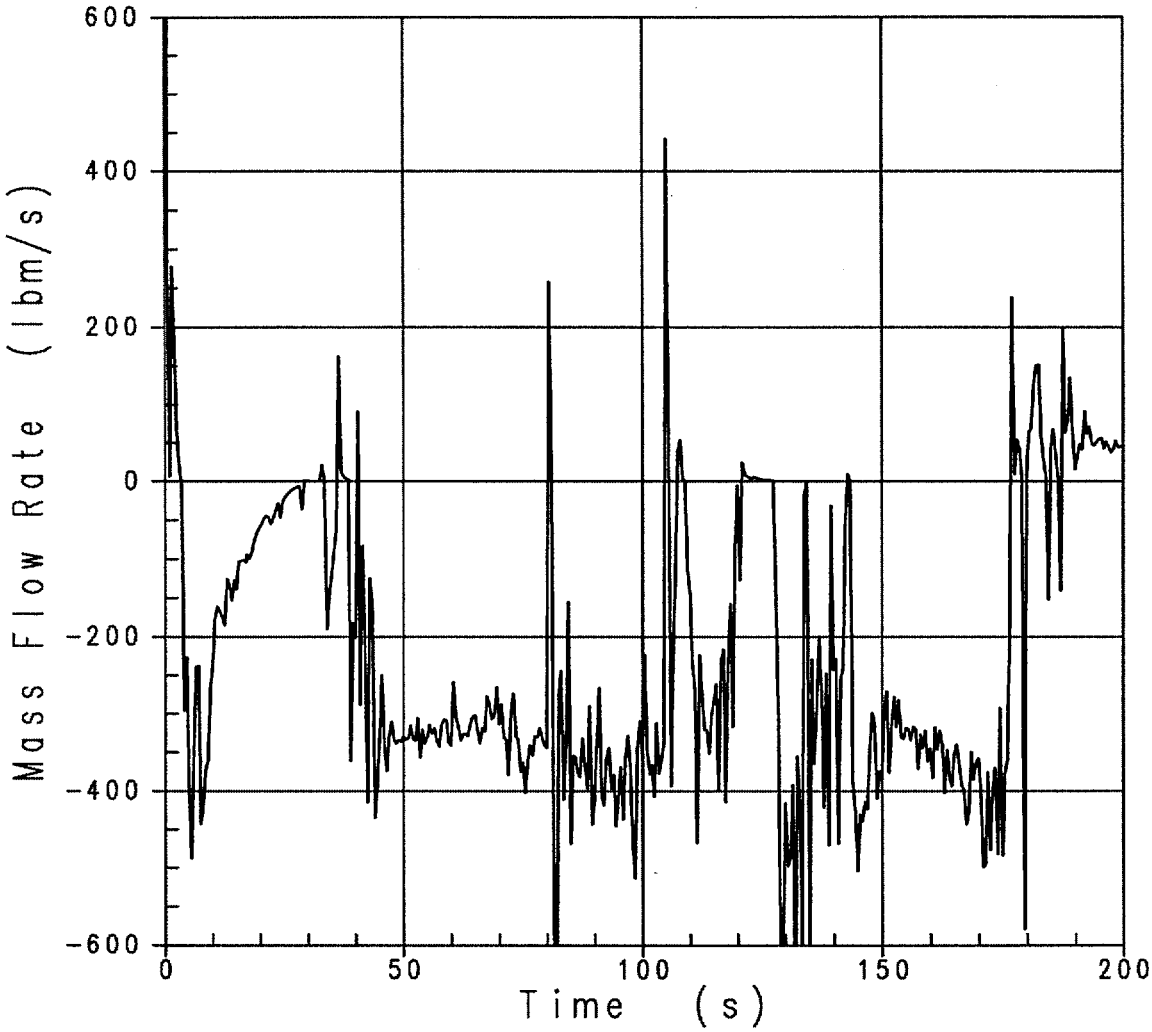
Superbounded Calculation
Vapor Flow at Top of Core Channel 11 (GT)
Figure 14.3.2-36



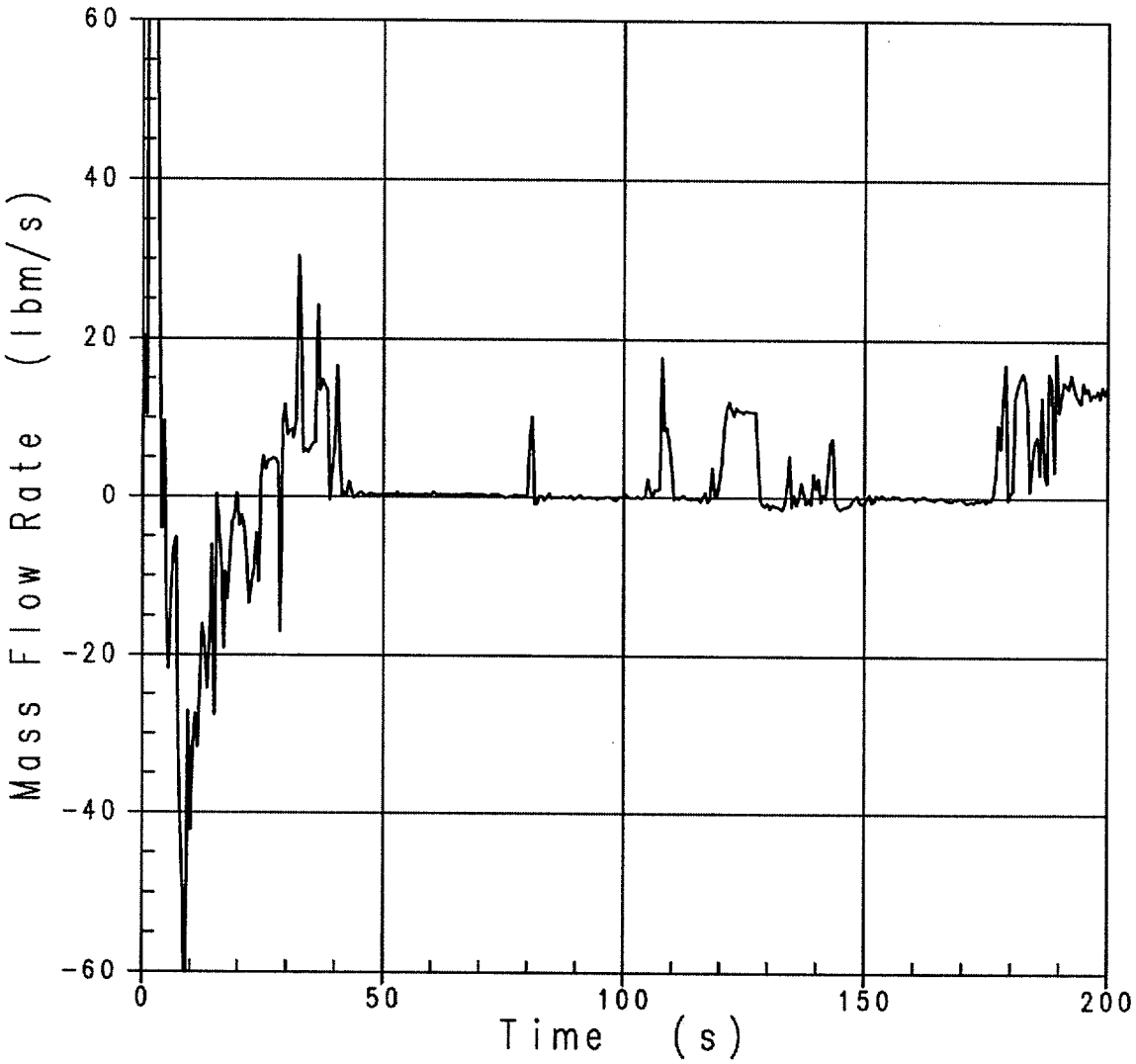
Superbounded Calculation
Liquid Flow at Top of Core Channel 12 (HA)
Figure 14.3.2-37



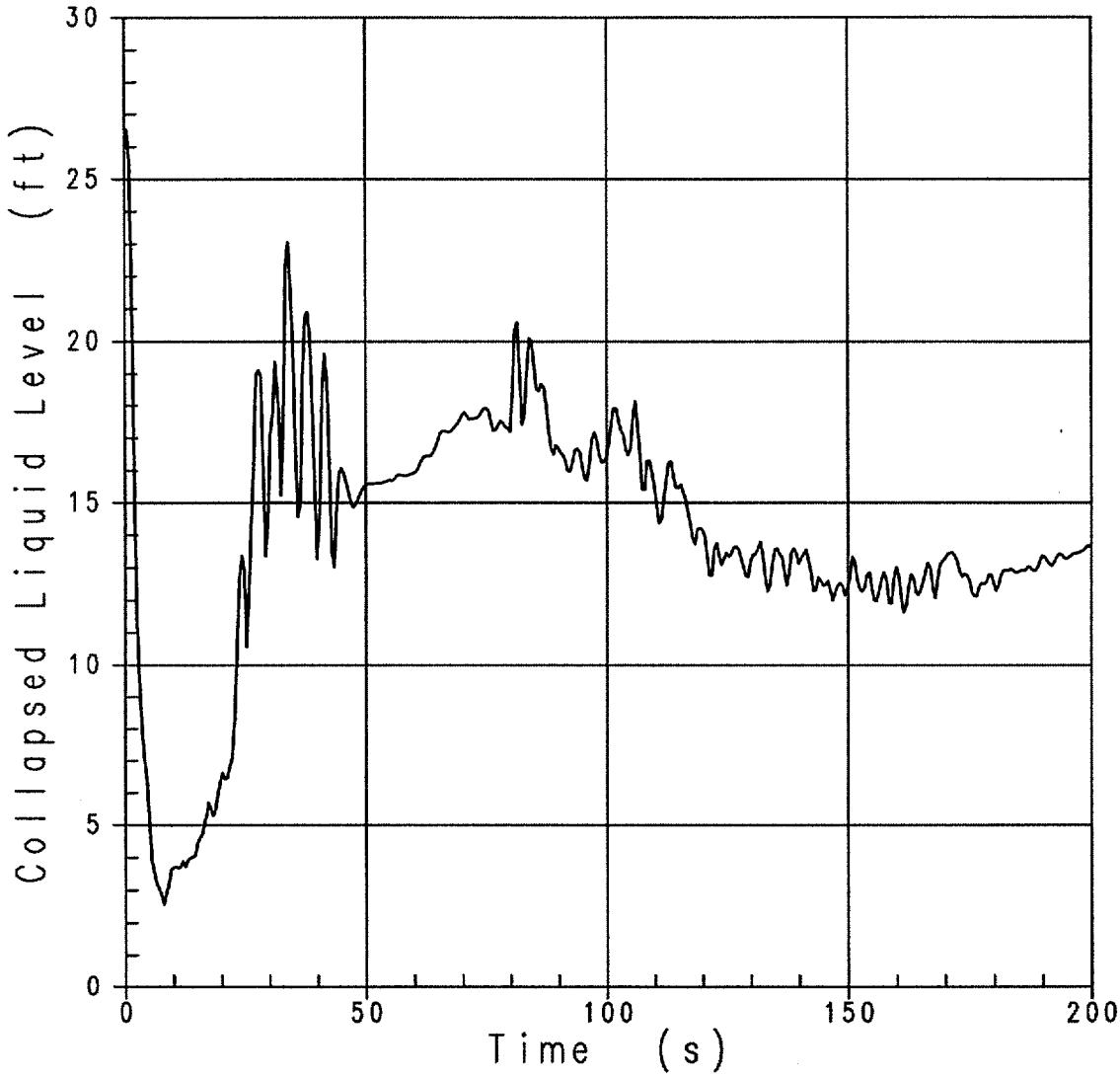
Superbounded Calculation
Vapor Flow at Top of Core Channel 12 (HA)
Figure 14.3.2-38



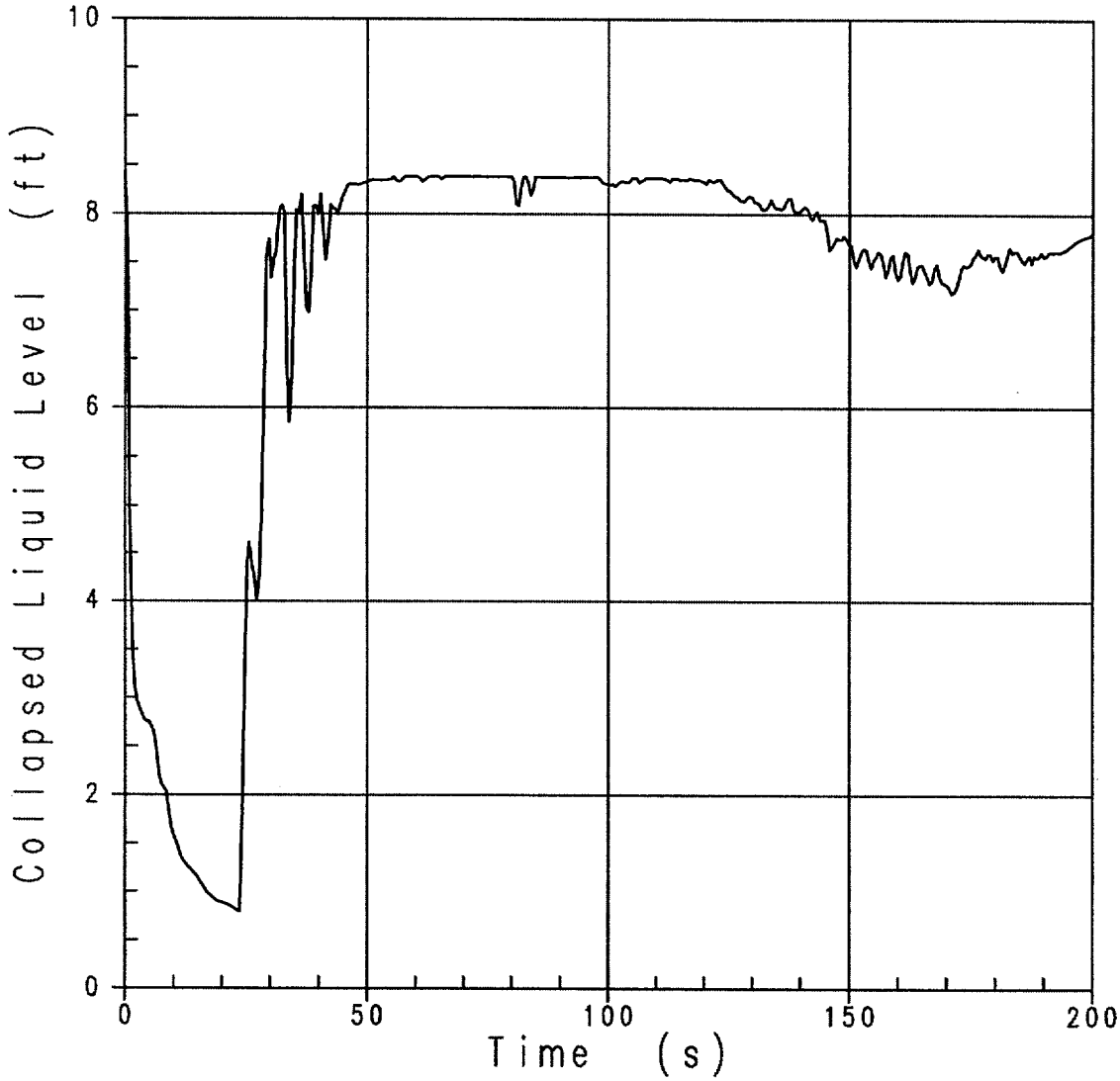
Superbounded Calculation
Liquid Flow at Top of Core Channel 13 (LP)
Figure 14.3.2-39



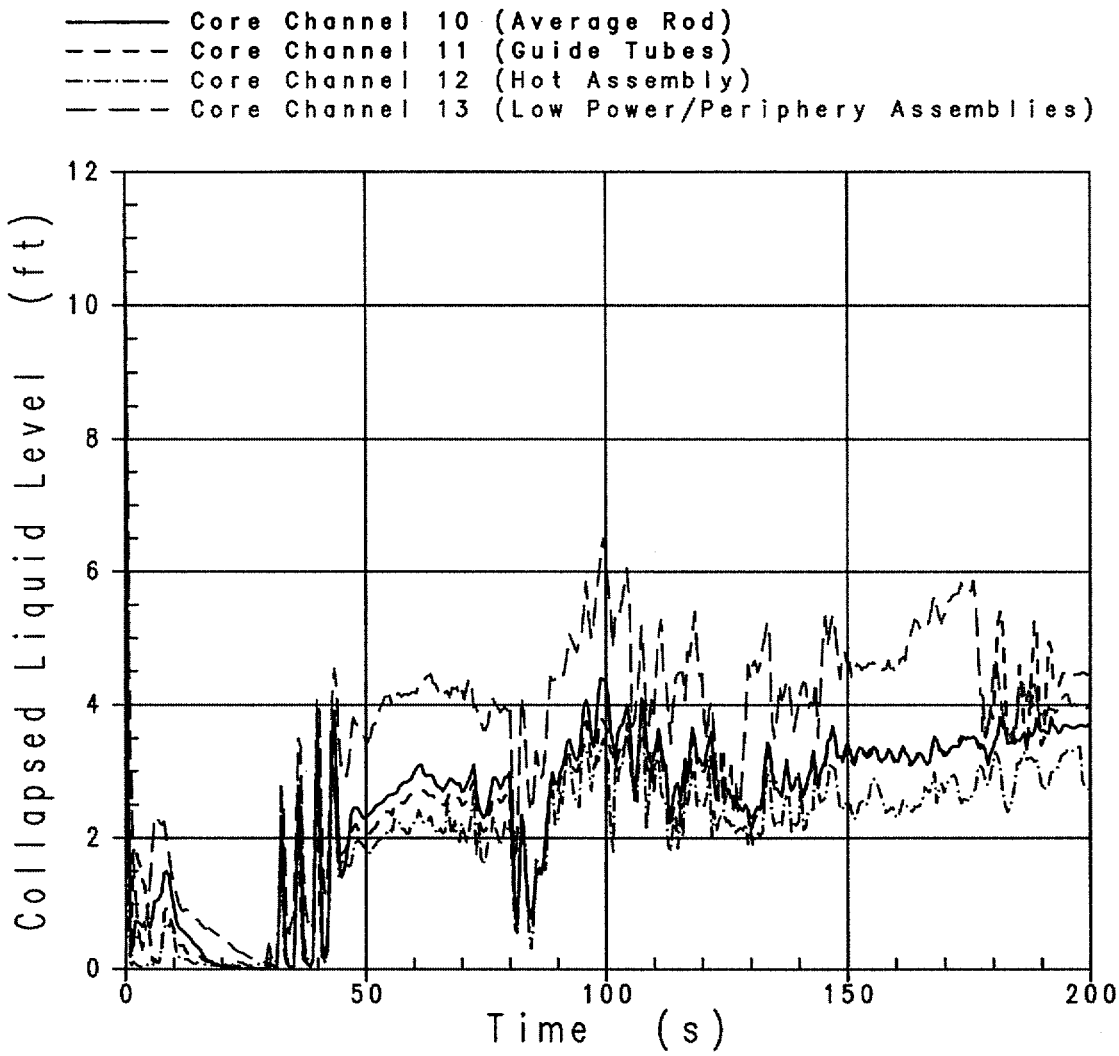
Superbounded Calculation
Vapor Flow at Top of Core Channel 13 (LP)
Figure 14.3.2-40



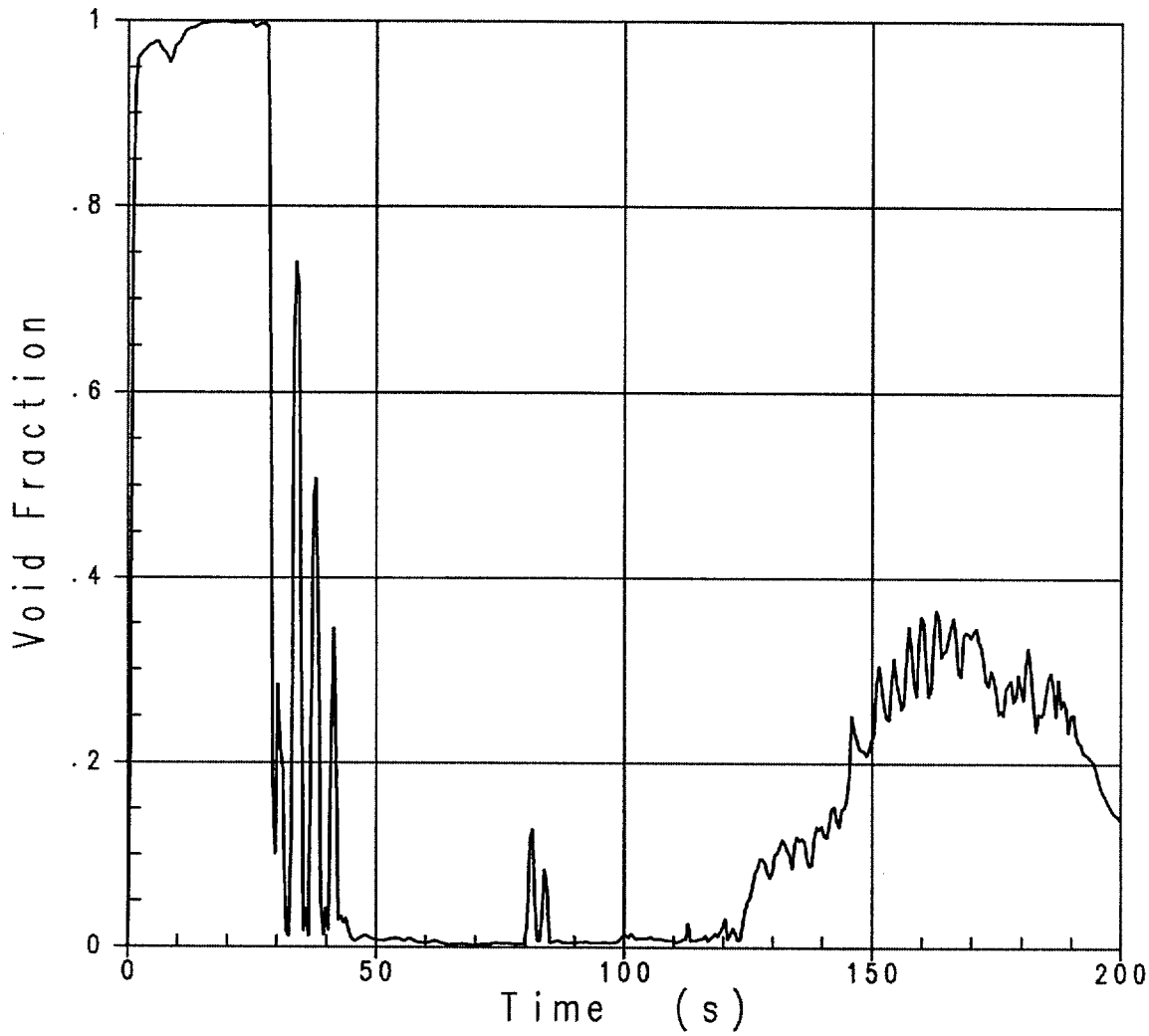
Superbounded Calculation
Downcomer Liquid Level
Figure 14.3.2-41



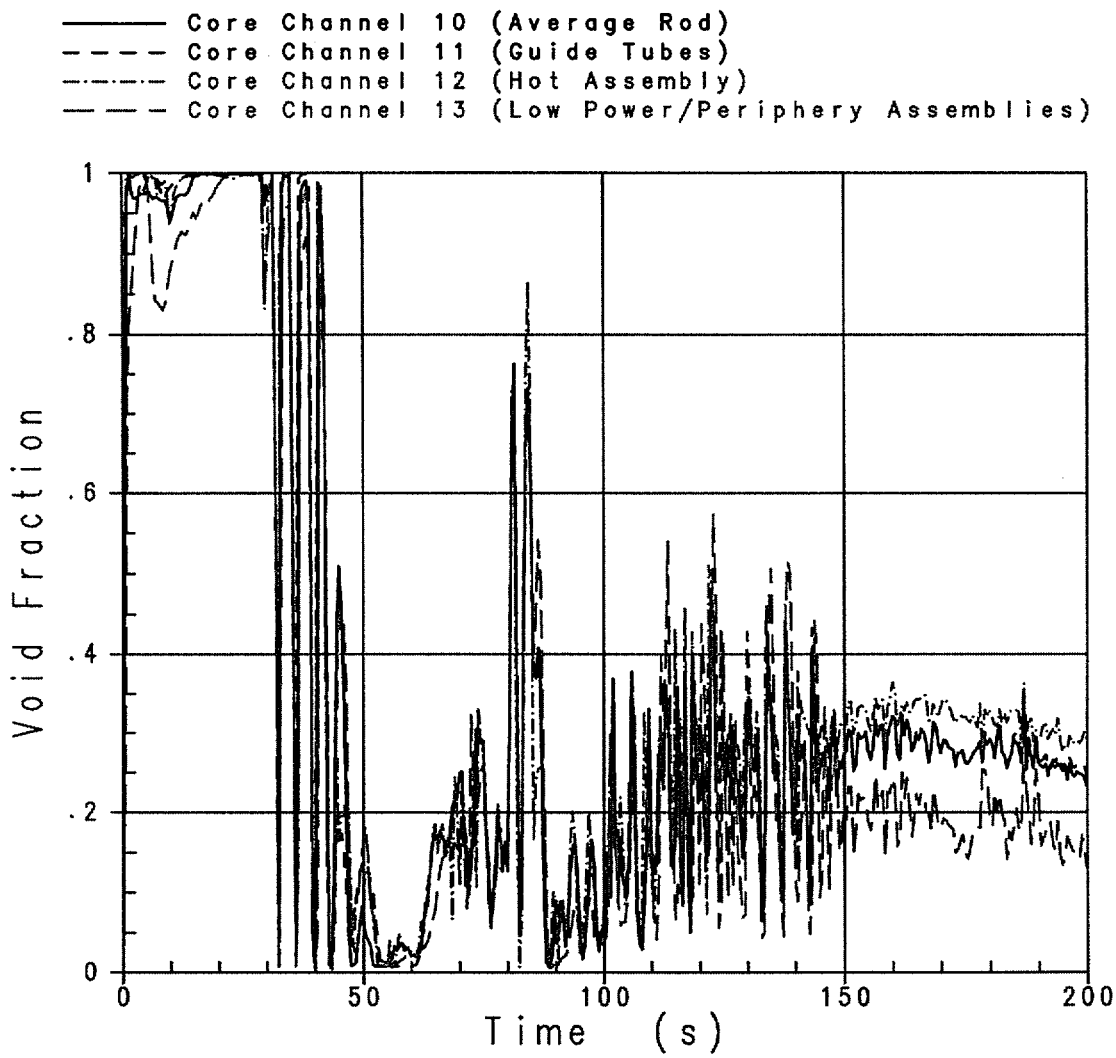
Superbounded Calculation
Lower Plenum Liquid Level
Figure 14.3.2-42



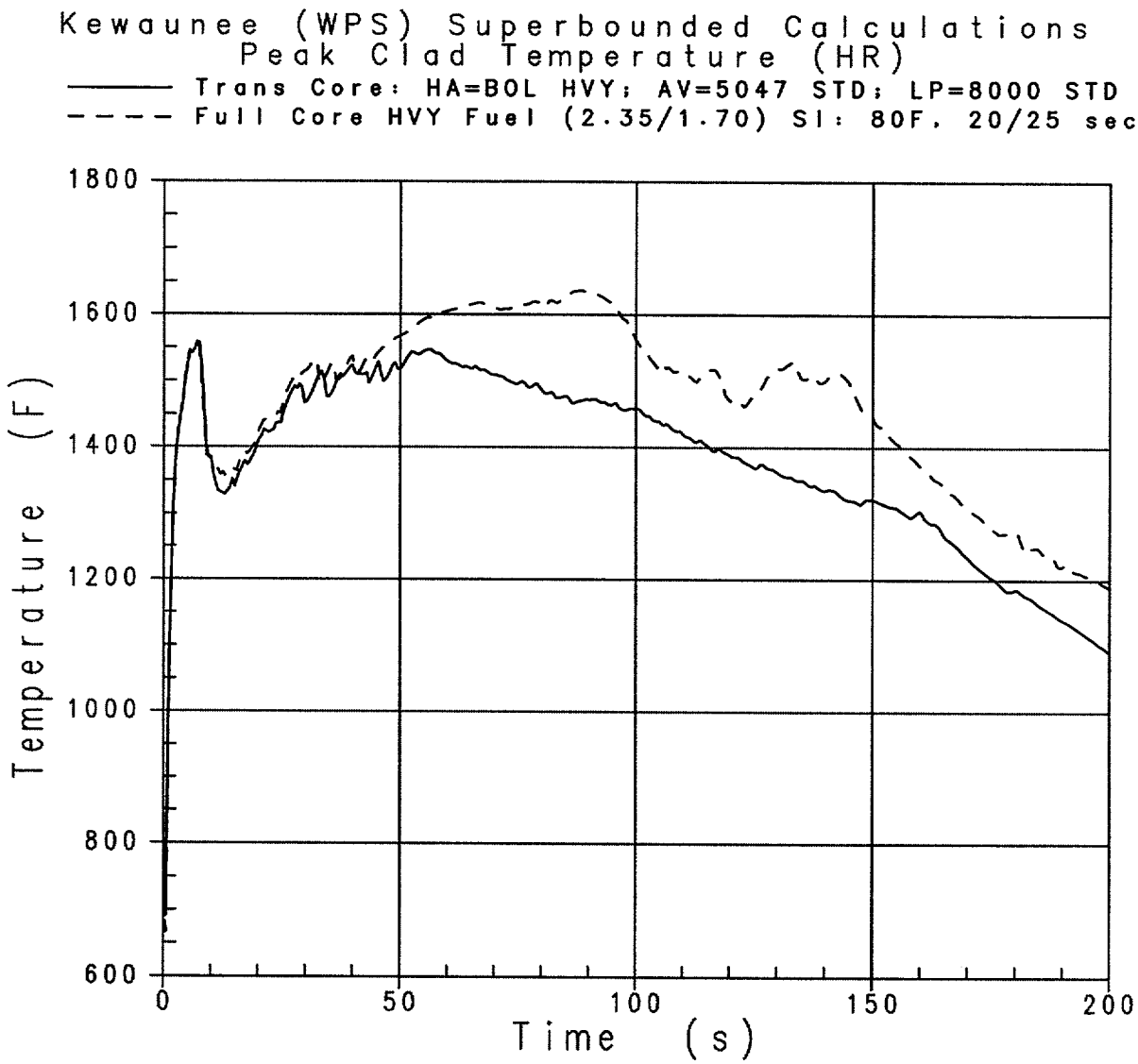
Superbounded Calculation
Core Liquid Level
Figure 14.3.2-43



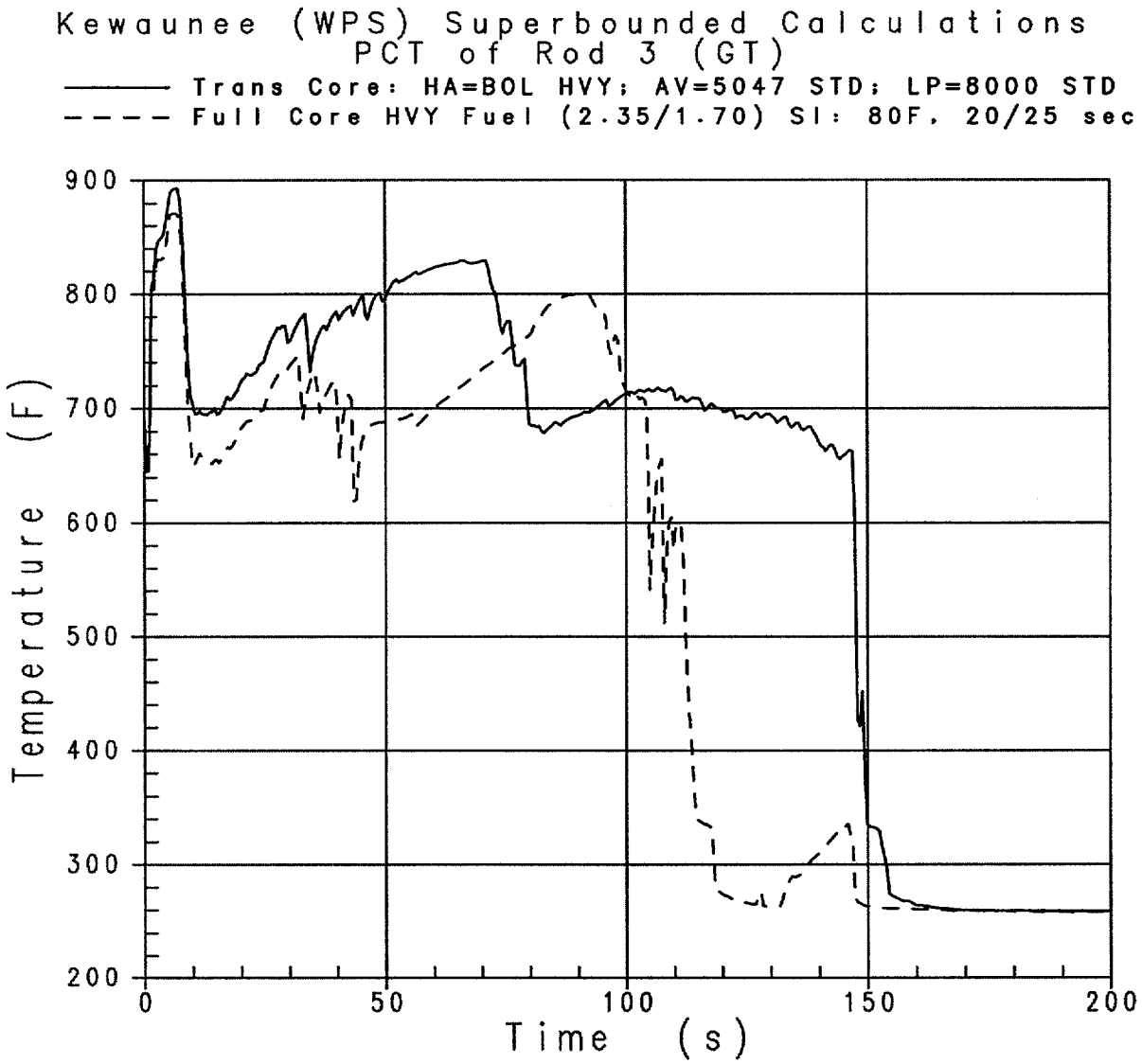
Superbounded Calculation
Void Fraction in Lower Plenum
Figure 14.3.2-44



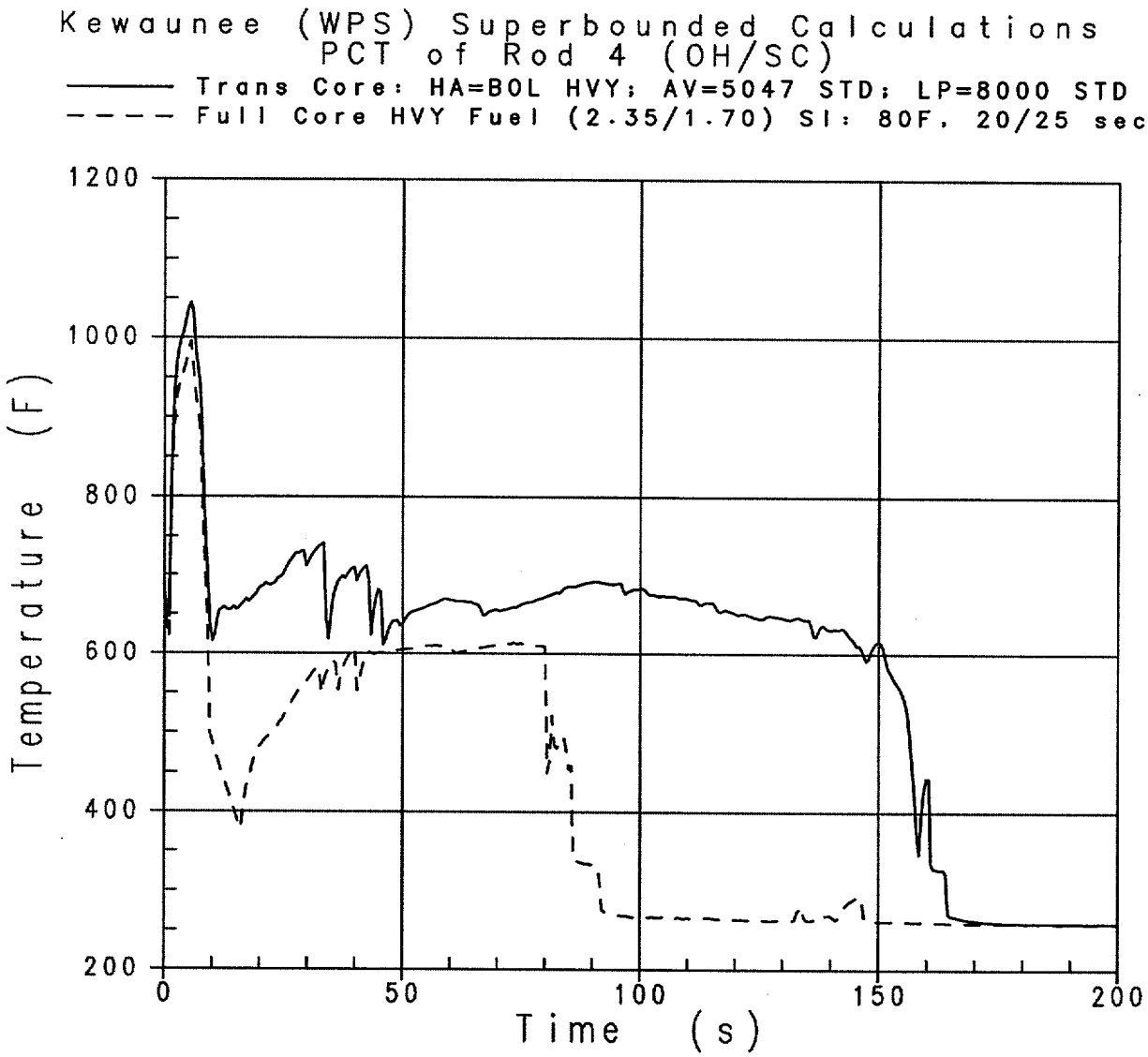
Superbounded Calculation
Void Fraction at Bottom of Core
Figure 14.3.2-45



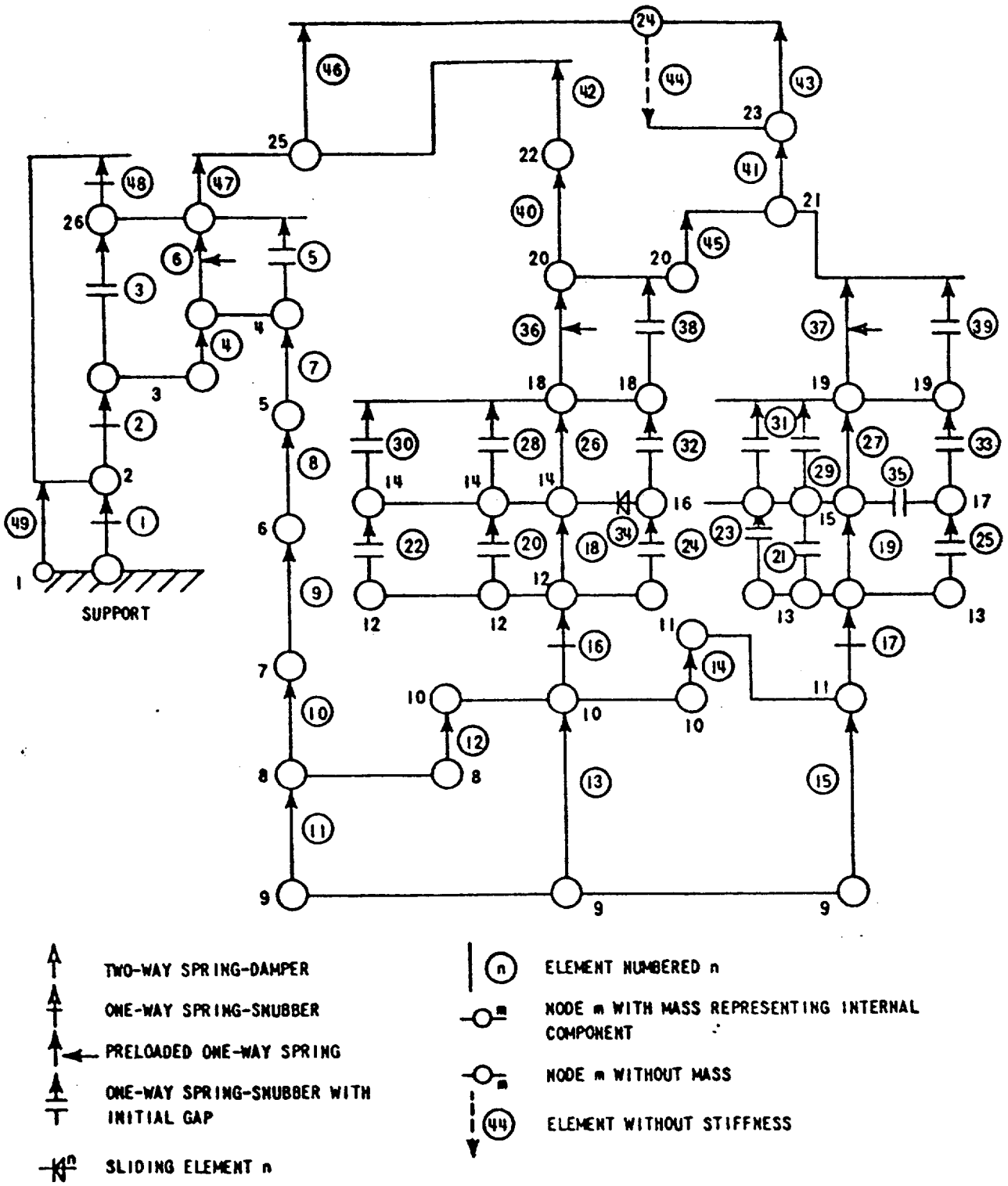
Peak Clad Temperature Comparison
Superbounded Calculations without Uncertainties
Figure 14.3.2-46



GT Assembly Clad Temperature Comparison
Superbounded Calculations without Uncertainties
Figure 14.3.2-47



OH/SC Assembly Clad Temperature Comparison
Superbounded Calculations without Uncertainties
Figure 14.3.2-48



LOOP REACTOR MATHEMATICAL MODEL FOR VERTICAL RESPONSE

FIGURE 14.3-21

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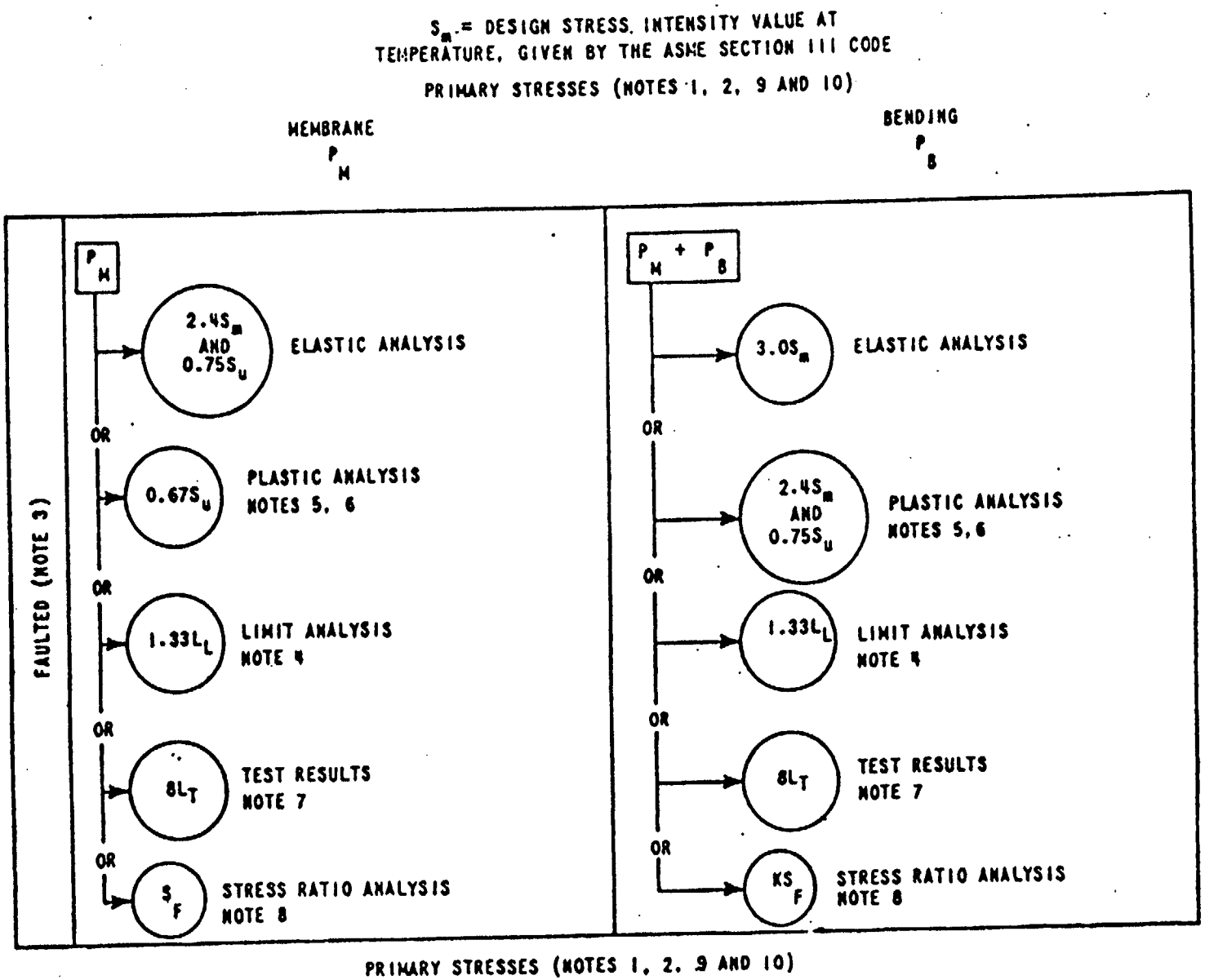

REACTOR INTERNALS ALLOWABLE STRESS CRITERIA

FIGURE 14.3-21a

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Notes For Figure 14.3-21a

Note 1. The symbols P_M, P_B do not represent single quantities but rather sets of six quantities representing the six quantities representing the six stress components σ_t, σ_l, σ_r, τ_{lr}, τ_{rt} and τ_{tl}.

Note 2. When loads are dynamically applied, consideration should be given to the use of dynamic load amplification and possible changes in modulus or elasticity.

Note 3. For configurations where compressive stresses occur, the stress limits shall be revised to take into account critical buckling stresses. The permissible equivalent static external pressure shall be taken as 2.5 times that given by the rules of Section III Pressure Vessel ASME Code. Where dynamic pressures are involved, the permissible external pressure shall be limited to 75 percent of the dynamic instability pressures.

Note 4. L_L = Lower bound limit load with an assumed yield point equal to 1.5 S_m. The "lower bound limit load" is here defined as that produced from the analysis of an ideally plastic (non-strain hardening) material where deformations increase with no further increase in applied load. The lower bound load is one in which the material everywhere satisfies equilibrium and nowhere exceeds the defined material yield strength using either a shear theory or a strain energy of distortion theory to relate multiaxial yielding to the uniaxial case.

Note 5. S_u = Ultimate strength at temperature. Multi-axiality effects on uniform strength shall be considered.

Note 6. Elastic plastic evaluated nominal primary stress. Strain hardening of the material may be used for the actual stress strain curve at the temperature of loading or any approximation to the actual stress-strain curve which everywhere has a

FIGURE 14.3-21b

(Sheet 1 of 2)

lower stress for the same strain as the actual curve may be used. Either the shear or strain energy of distortion flow rule shall be used to account for multi-axial effects.

Note 7. The stress limits given in this criterion need not be satisfied if it can be shown from the test of a prototype or model that the specified loads do not exceed 80% of L_T, for Faulted conditions. L_T is the ultimate load or load combination used in the test. In using this method, account shall be taken of the dimensional tolerances which may exist between the actual part and the tested part or parts as well as differences which may exist in the ultimate strength or other governing material properties of the actual part and the tested parts to assure that the loads obtained from the test are a conservative representation of the load carrying capability of the actual component under postulated loading for Faulted Conditions.

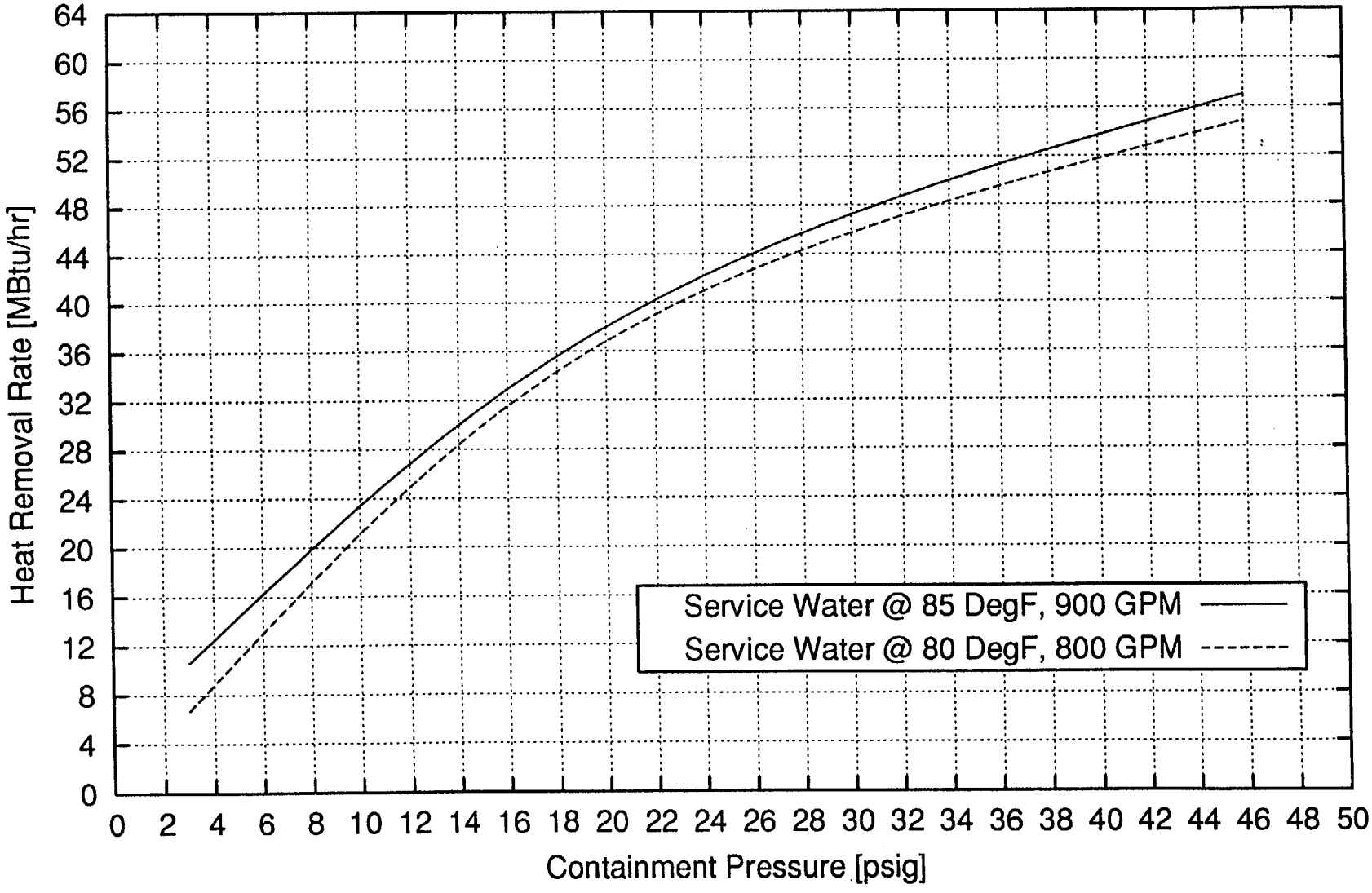
Note 8. Stress ratio is a method of plastic analysis which uses the stress ratio combinations (combination of stresses that consider the ratio of the actual stress to the allowable plastic or elastic stress) to compute the maximum load a strain hardening material can carry. For Faulted condition use $S_F < 2.4S_m$ and $0.75\ S_u$, whichever is smaller.

Note 9. Where deformation is of concern in a component, the deformation shall be limited to 80% of the value given in the Design Specification (no loss of function) for Faulted Conditions.

Note 10. No limit requirements are established for fatigue and secondary stresses, however evaluation of these conditions are recommended when safety requirements are involved.

FIGURE 14.3-21b

(Sheet 2 of 2)



CONTAINMENT FAN COOLER HEAT REMOVAL RATE vs. CONTAINMENT PRESSURE
FIGURE 14.3-22

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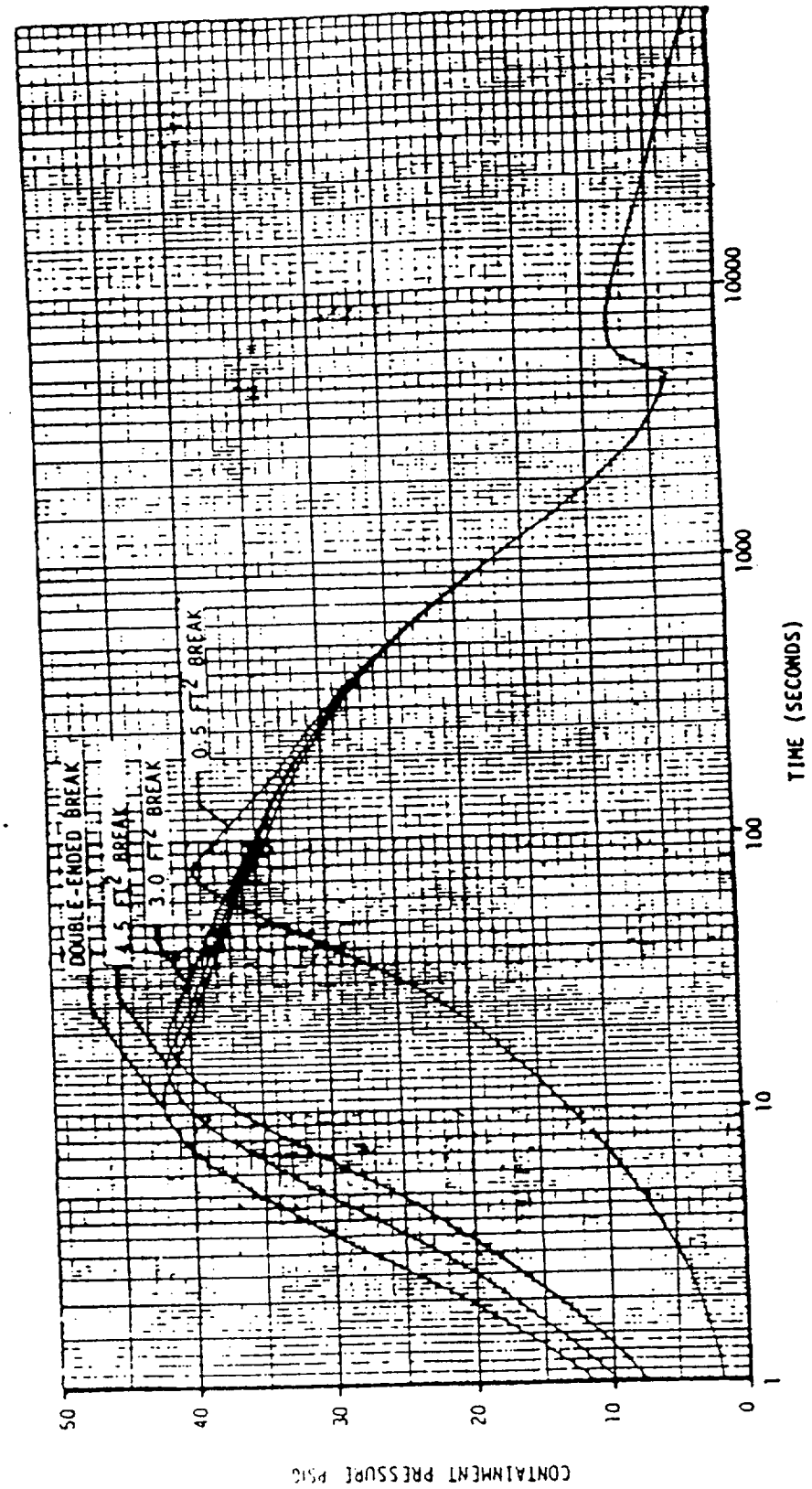

CONTAINMENT PRESSURE TRANSIENT

FIGURE 14.3-23

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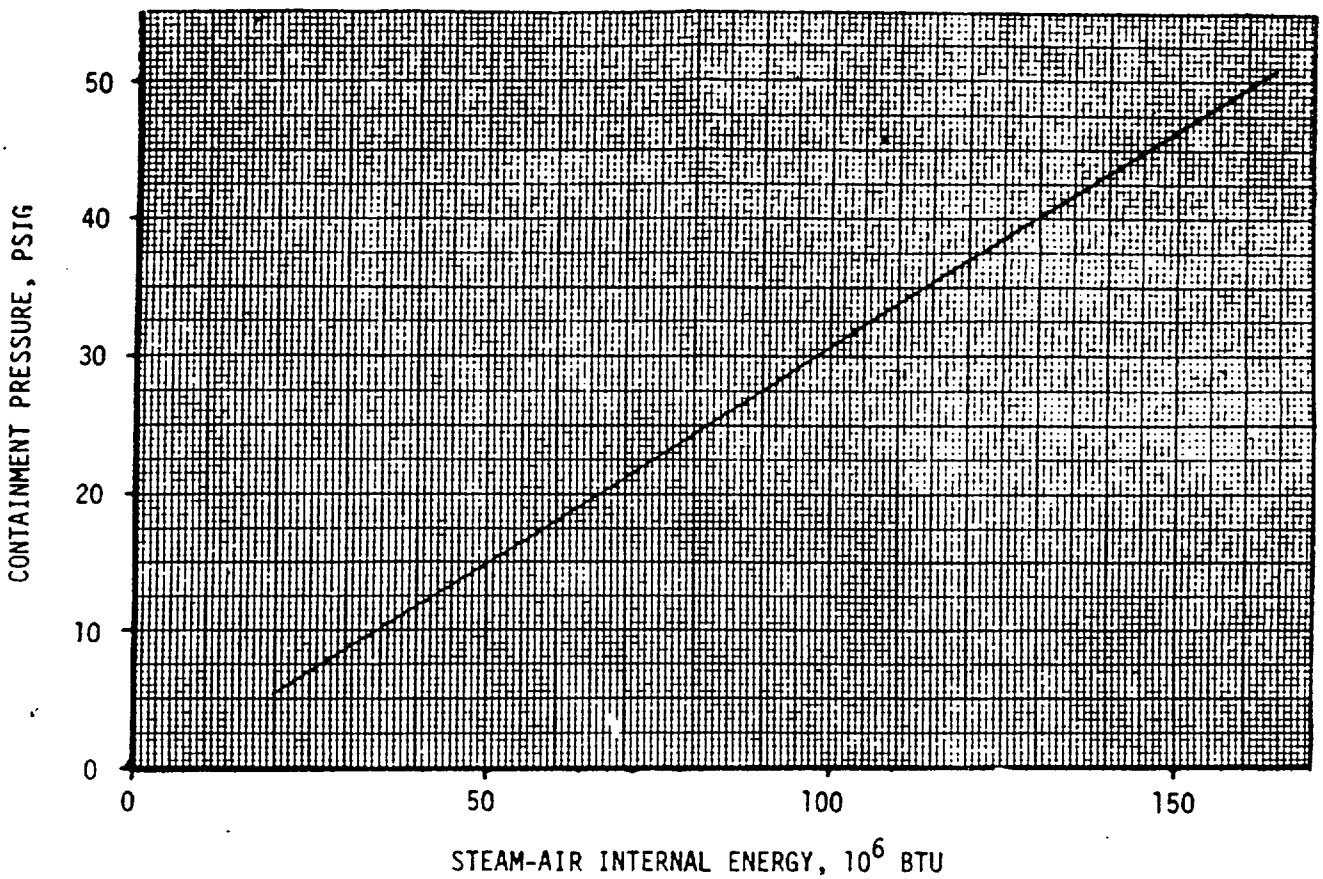

CONTAINMENT CAPABILITY STUDY CONTAINMENT PRESSURE VS STEAM-AIR INTERNAL ENERGY VOLUME = 1.32×10^6 ft^3

FIGURE 14.3-24

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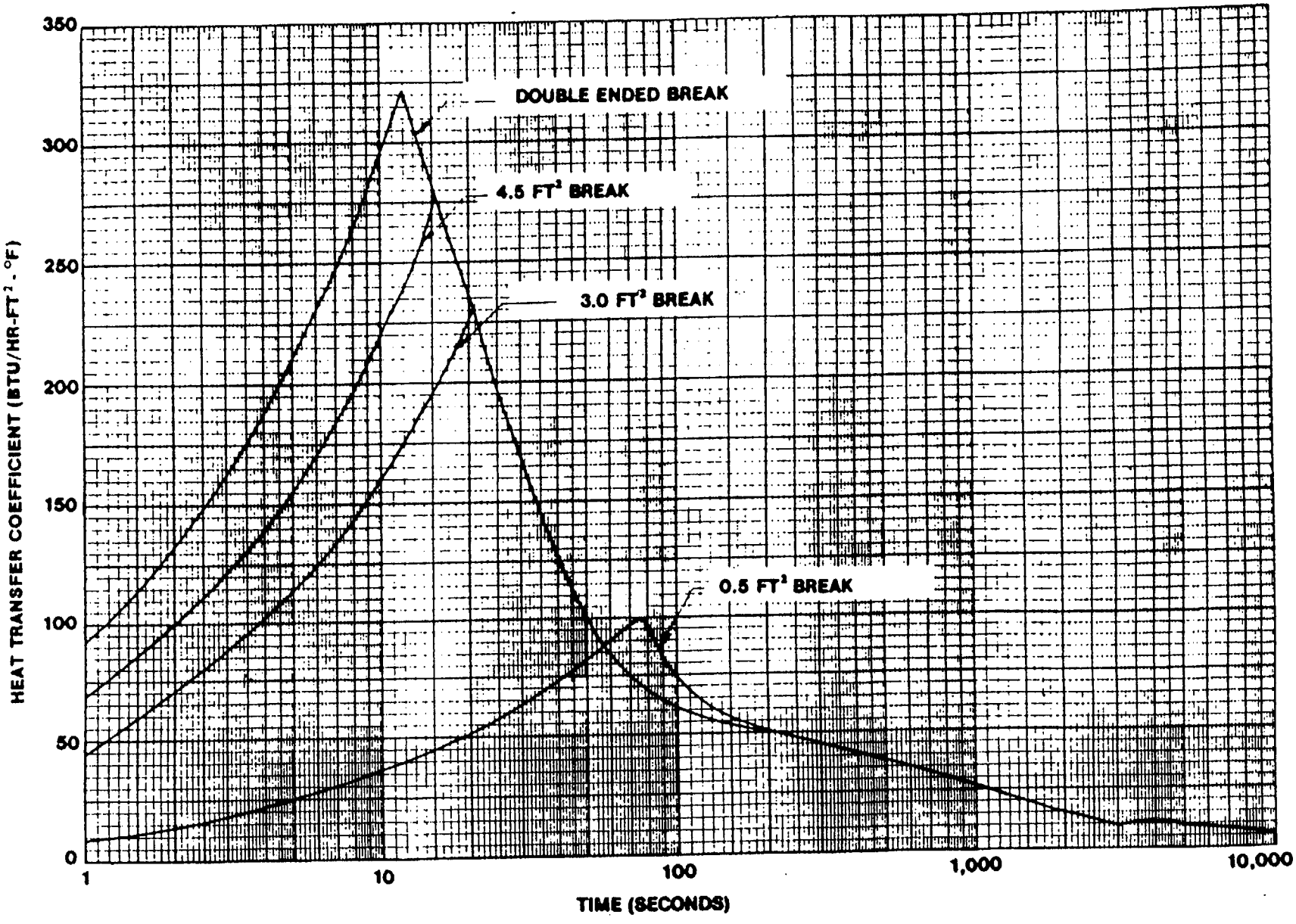

STRUCTURAL HEAT TRANSFER COEFFICIENT

FIGURE 14.3-25

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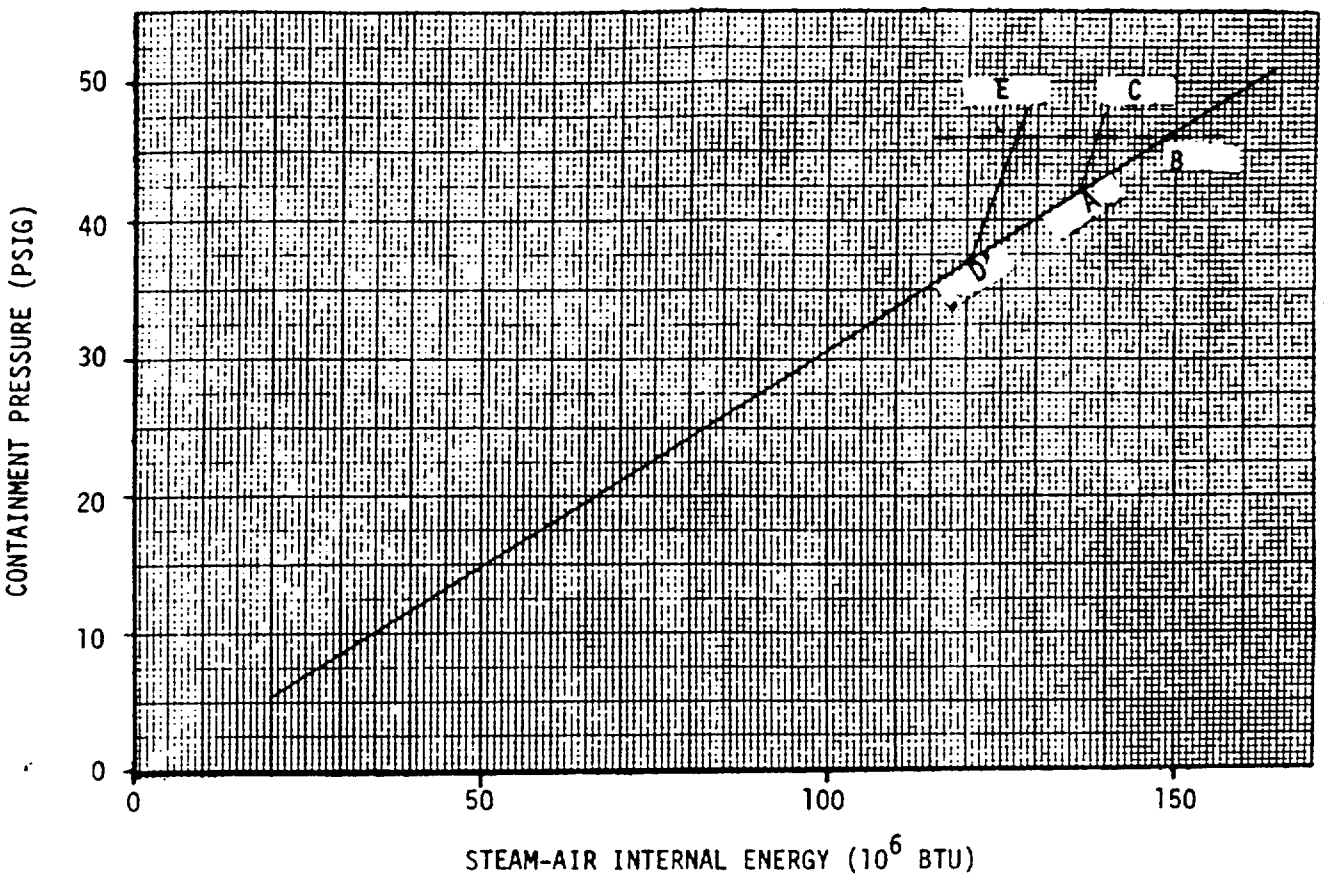

CONTAINMENT CAPABILITY STUDY CONTAINMENT PRESSURE VS STEAM-AIR INTERNAL ENERGY VOLUME = 1.32×10^6 ft^3

FIGURE 14.3-26

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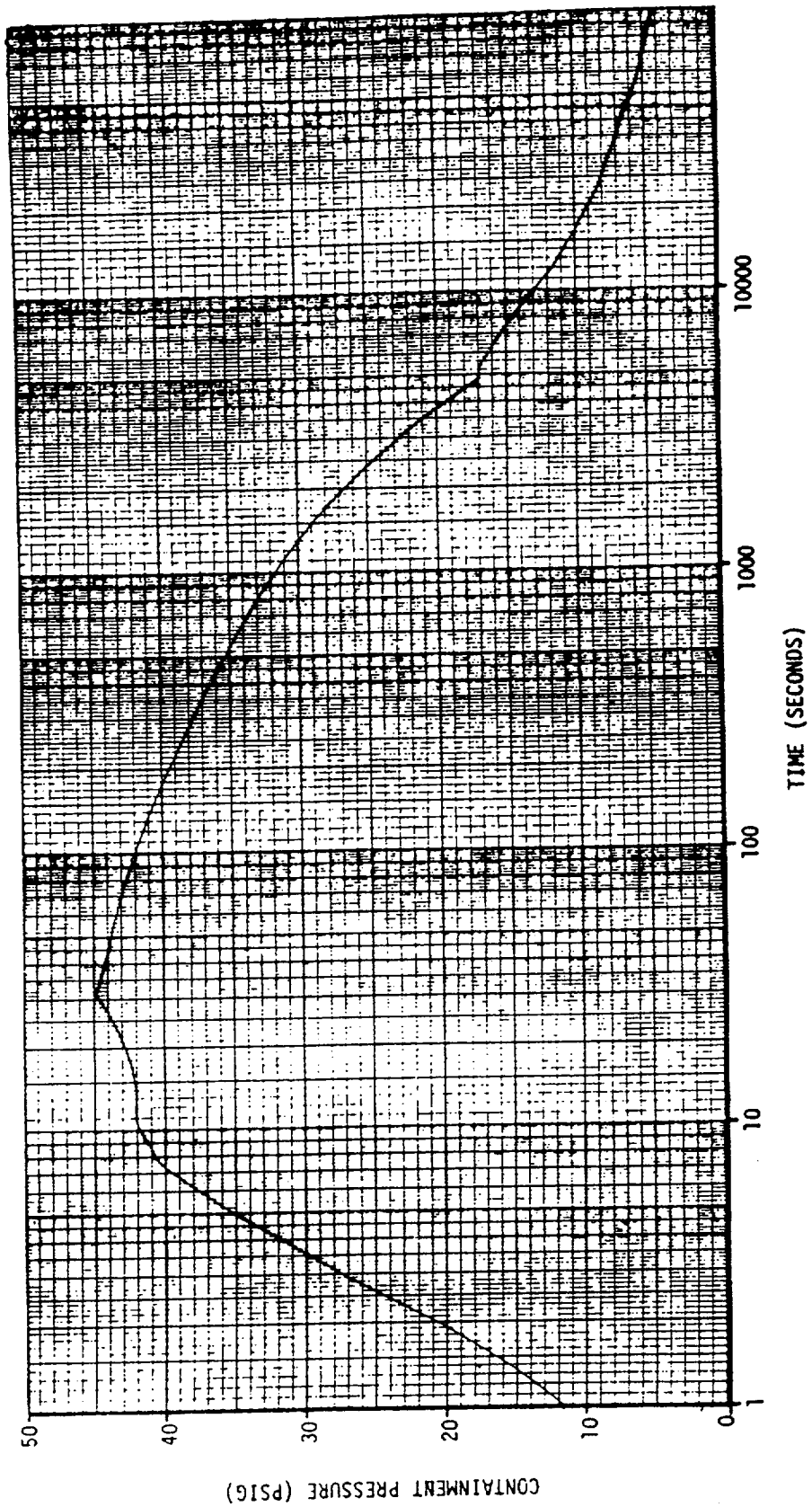

CONTAINMENT CAPABILITY STUDY ALL AVAILABLE ENERGIES

FIGURE 14.3-27

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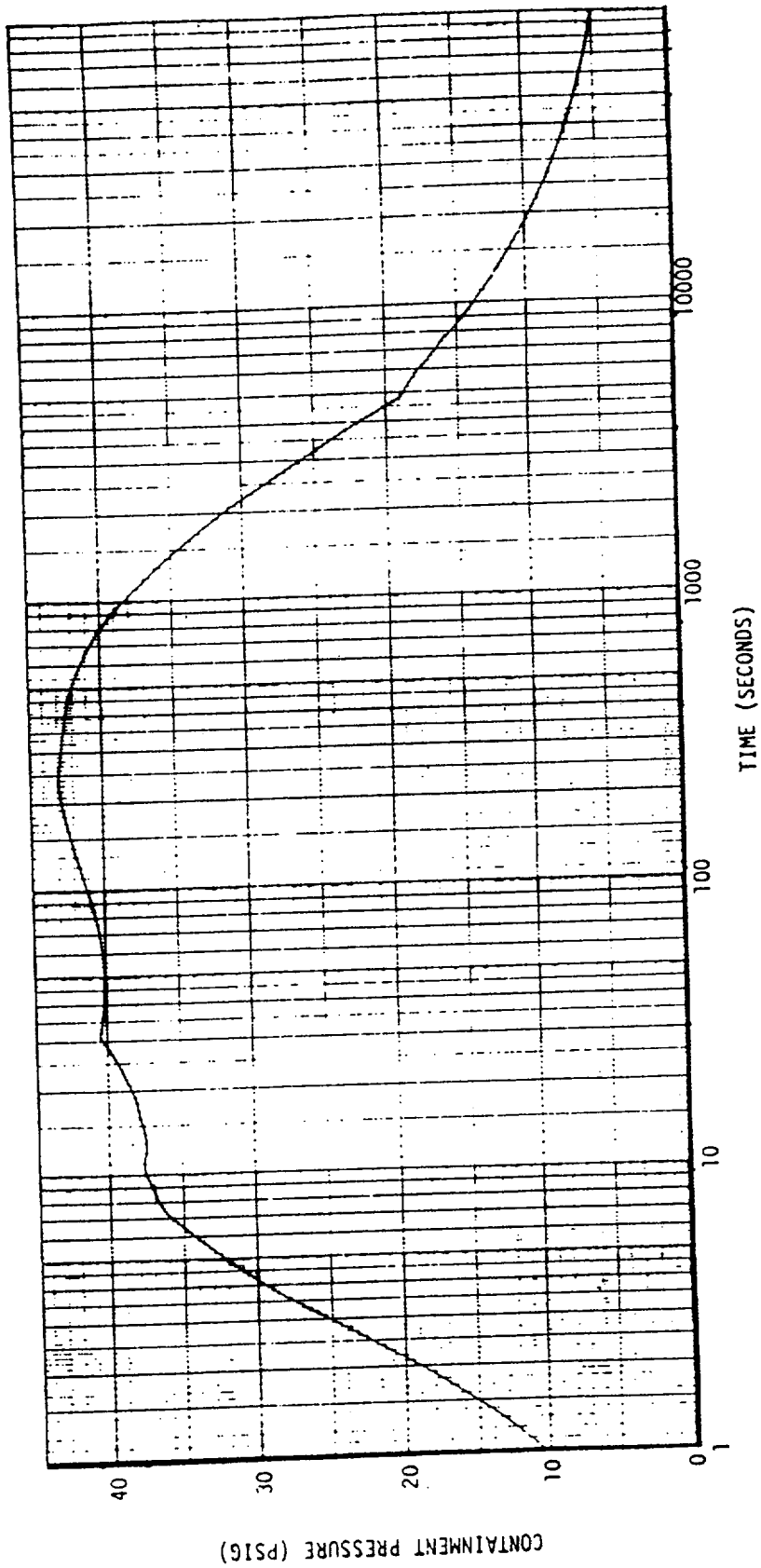

CONTAINMENT CAPABILITY STUDY Zr-H_2O REACTION (32.3%)

FIGURE 14.3-28

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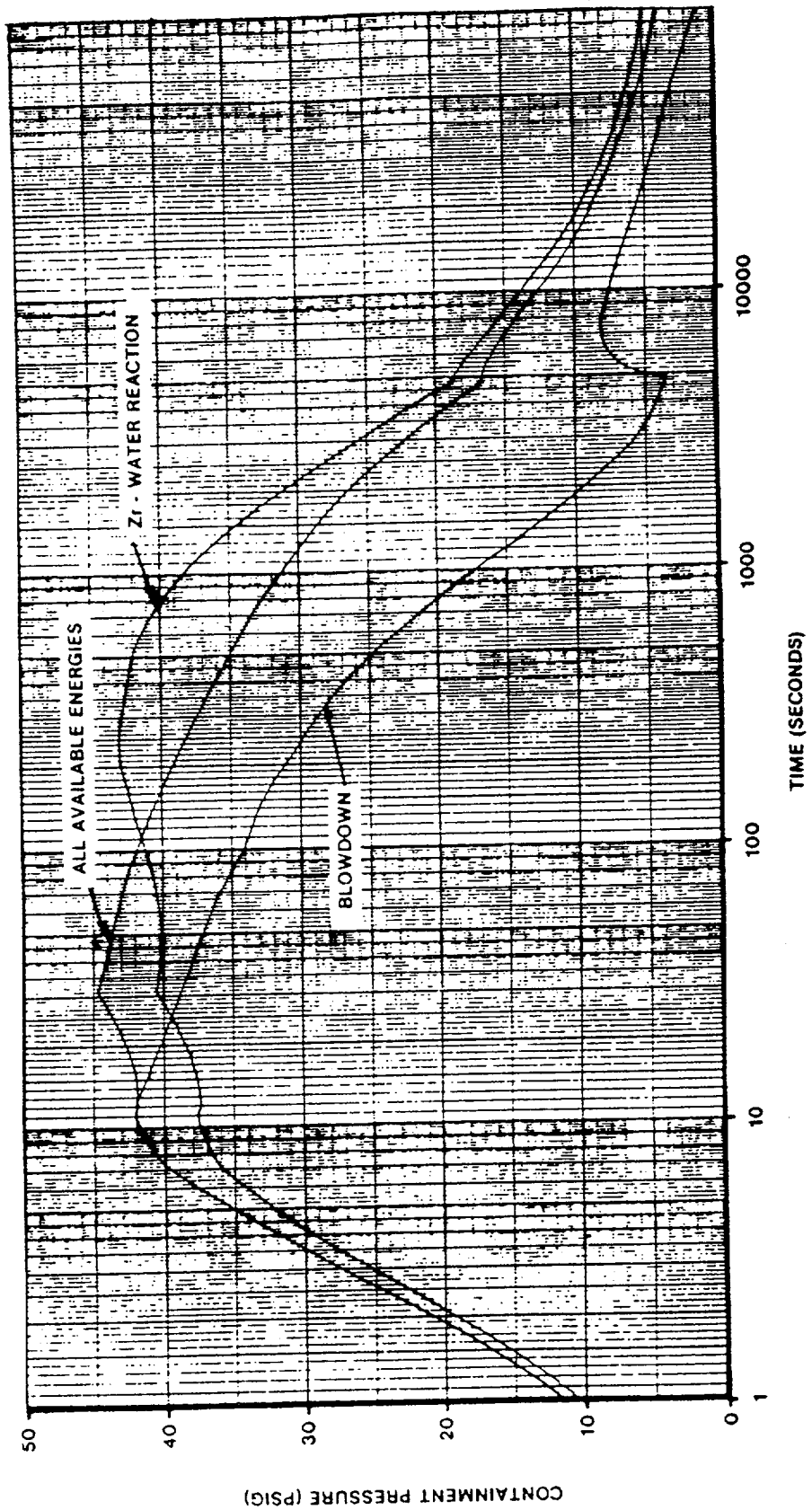

CONTAINMENT PRESSURE TRANSIENTS

FIGURE 14.3-29

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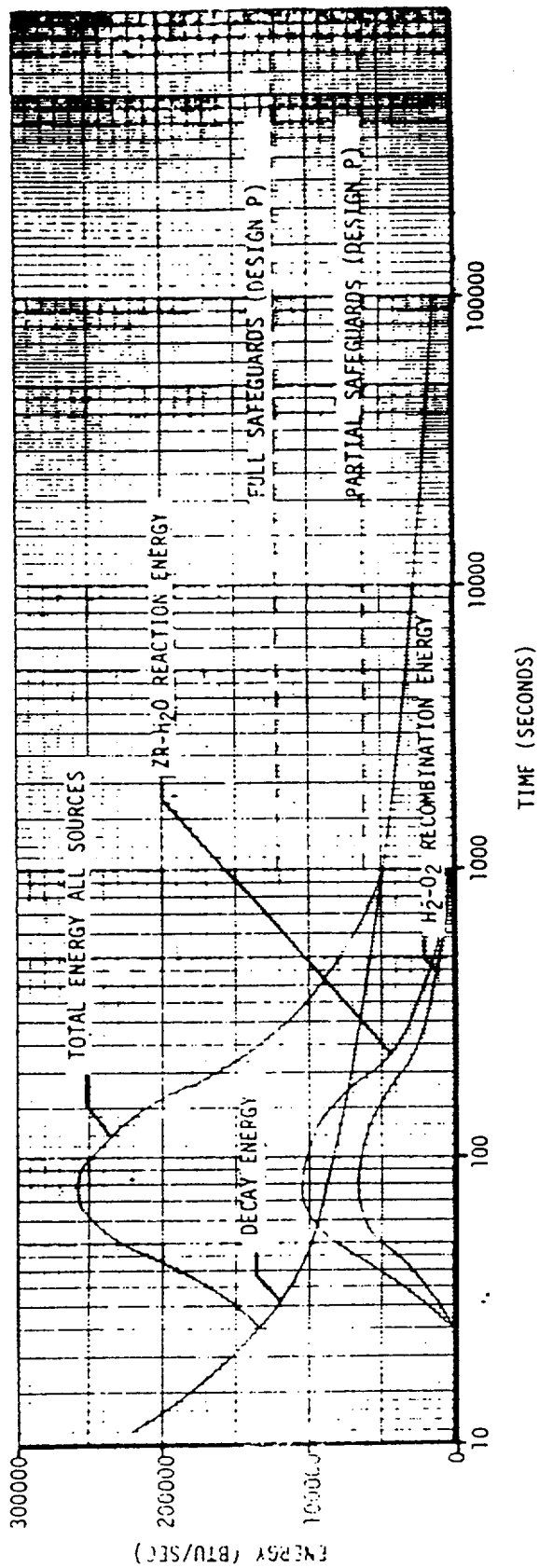

CONTAINMENT CAPABILITY STUDY RATE OF ENERGY ADDITION

FIGURE 14.3-30

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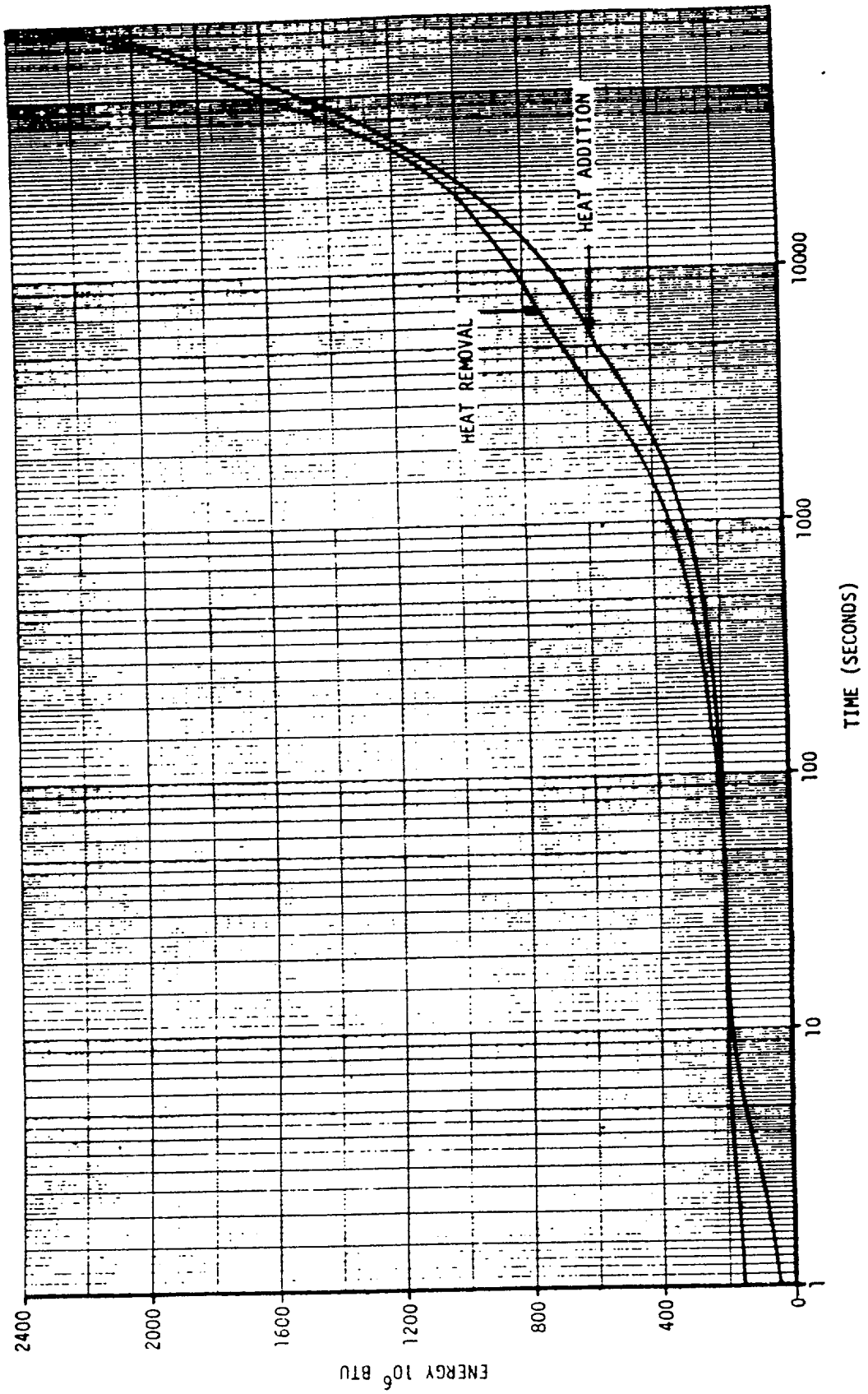

CONTAINMENT CAPABILITY - CASE 1

FIGURE 14.3-31

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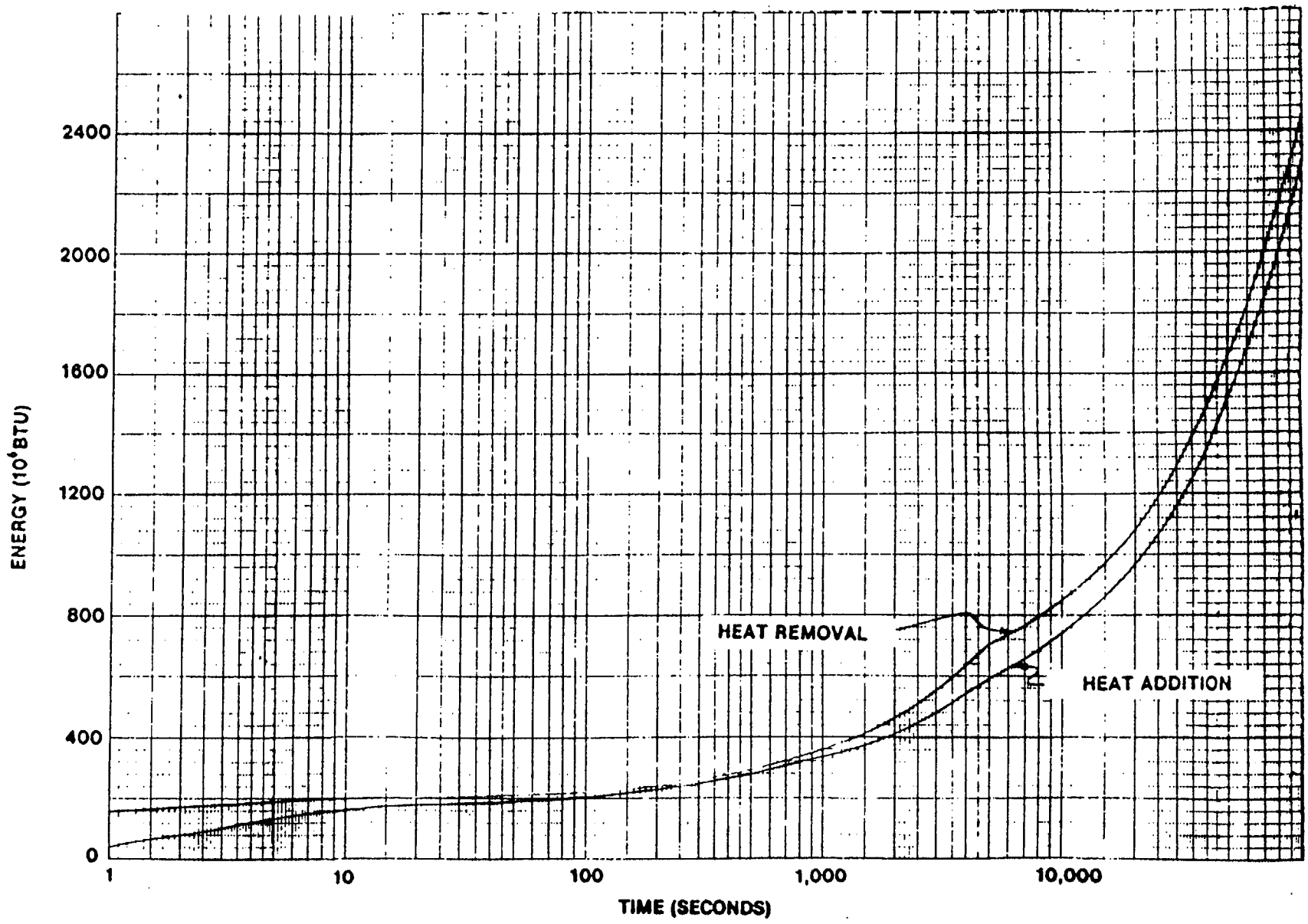

CONTAINMENT CAPABILITY - CASE 2

FIGURE 14.3-32

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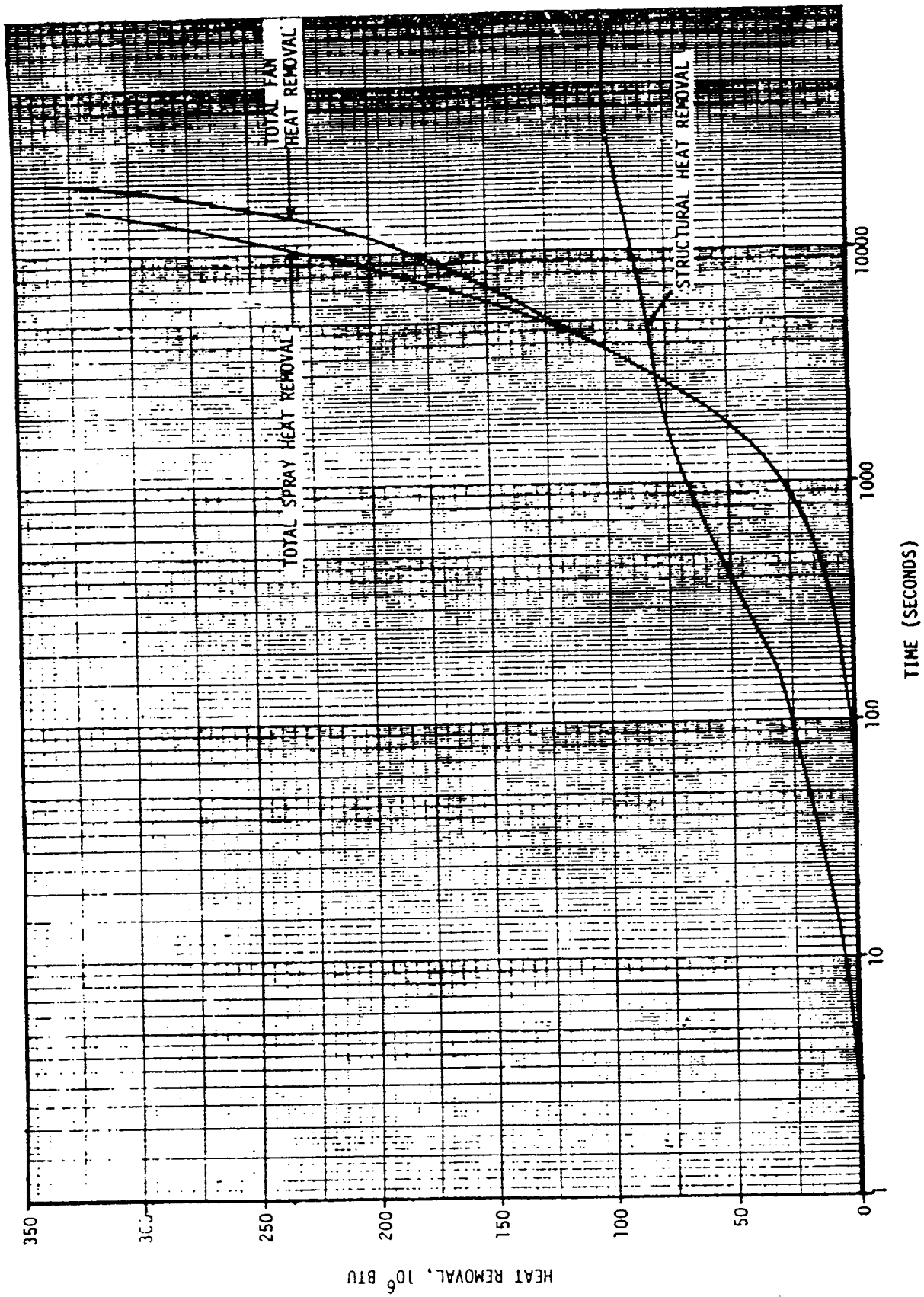

INDIVIDUAL CONTRIBUTIONS TO HEAT REMOVAL

FIGURE 14.3-33

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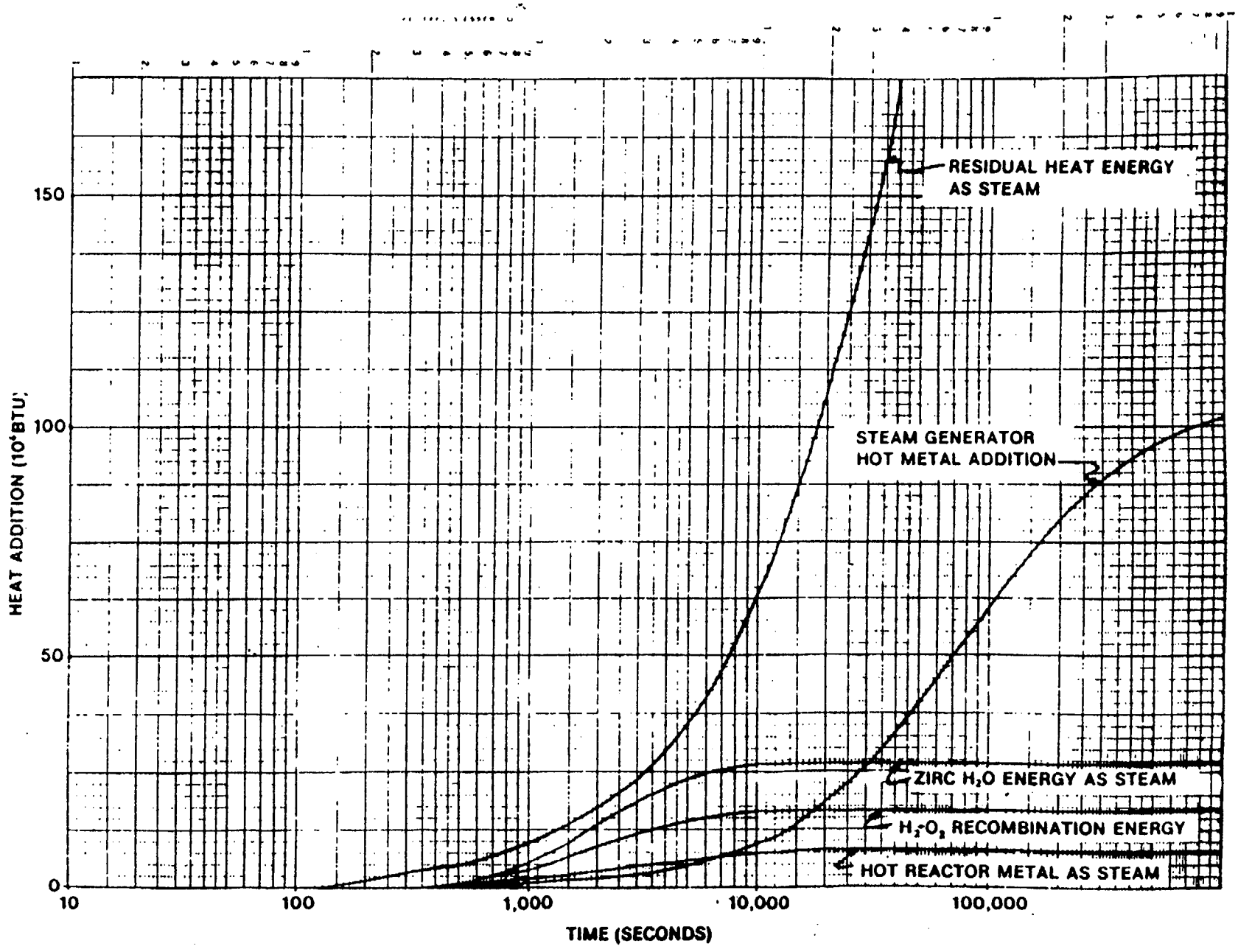

CONTAINMENT CAPABILITY STUDY HEAT SOURCE

FIGURE 14.3-34

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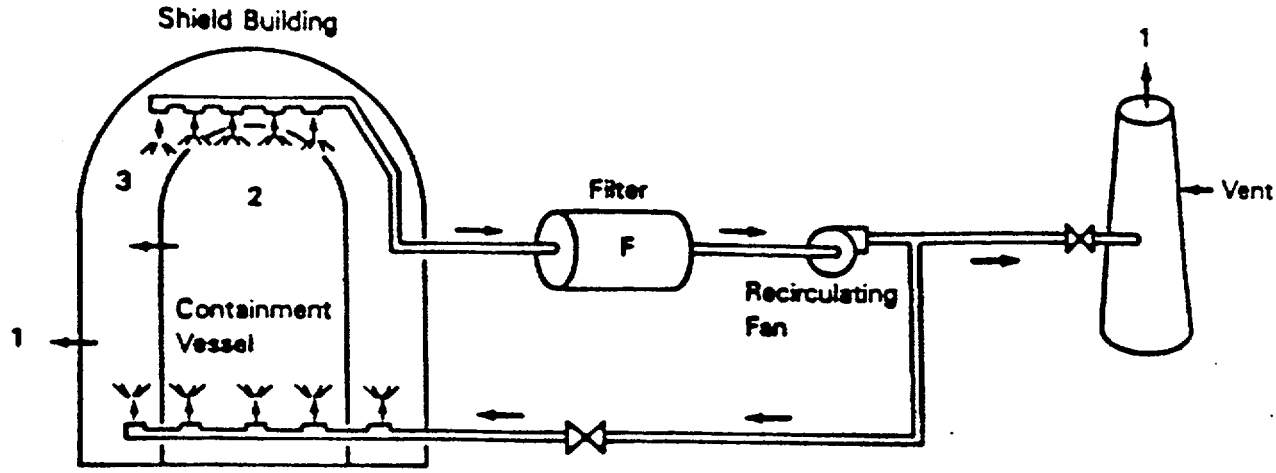
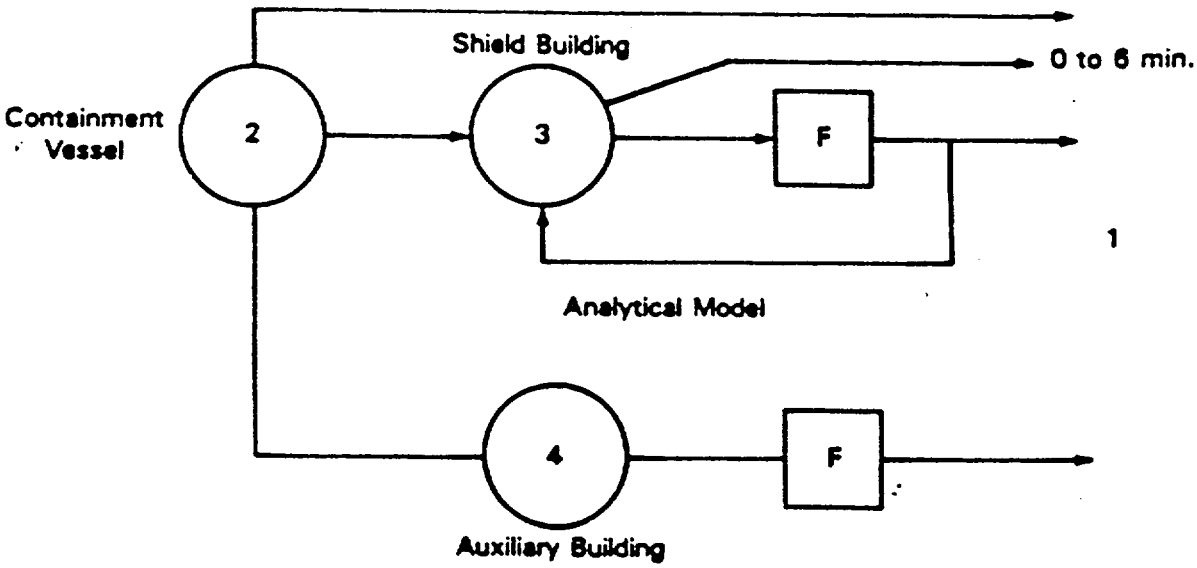


System Schematic



Analytical Model

LOCADOSE
Regions

1. Environment
2. Containment Vessel
3. Shield Building
4. Auxiliary Building Special Ventilation Zone

SHIELD BUILDING VENTILATION SYSTEM
FIGURE 14.3-35

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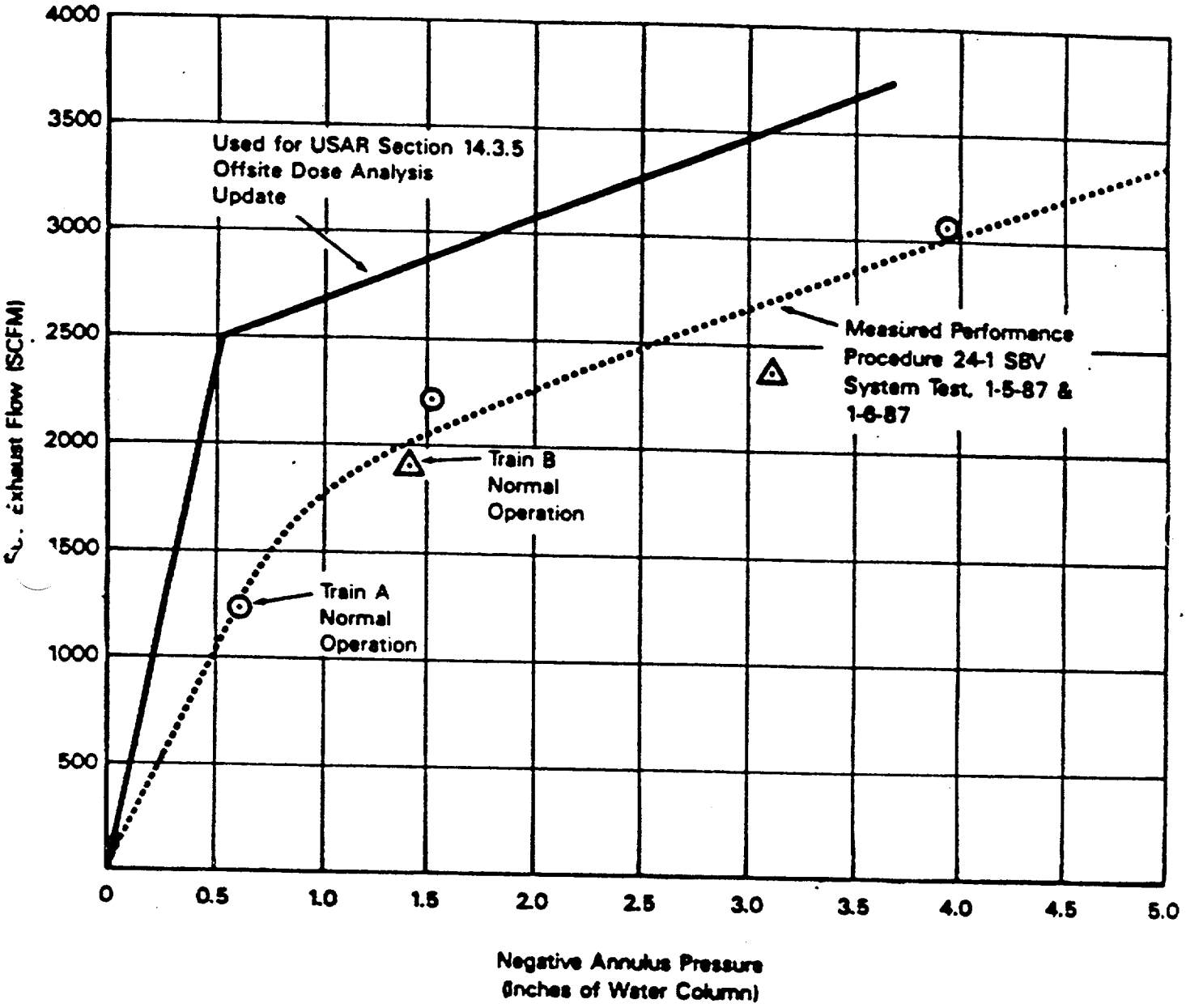

SHIELD BUILDING VENTILATION PERFORMANCE TEST CURVE
AND SHIELD BUILDING PERFORMANCE CURVE

FIGURE 14.3-36

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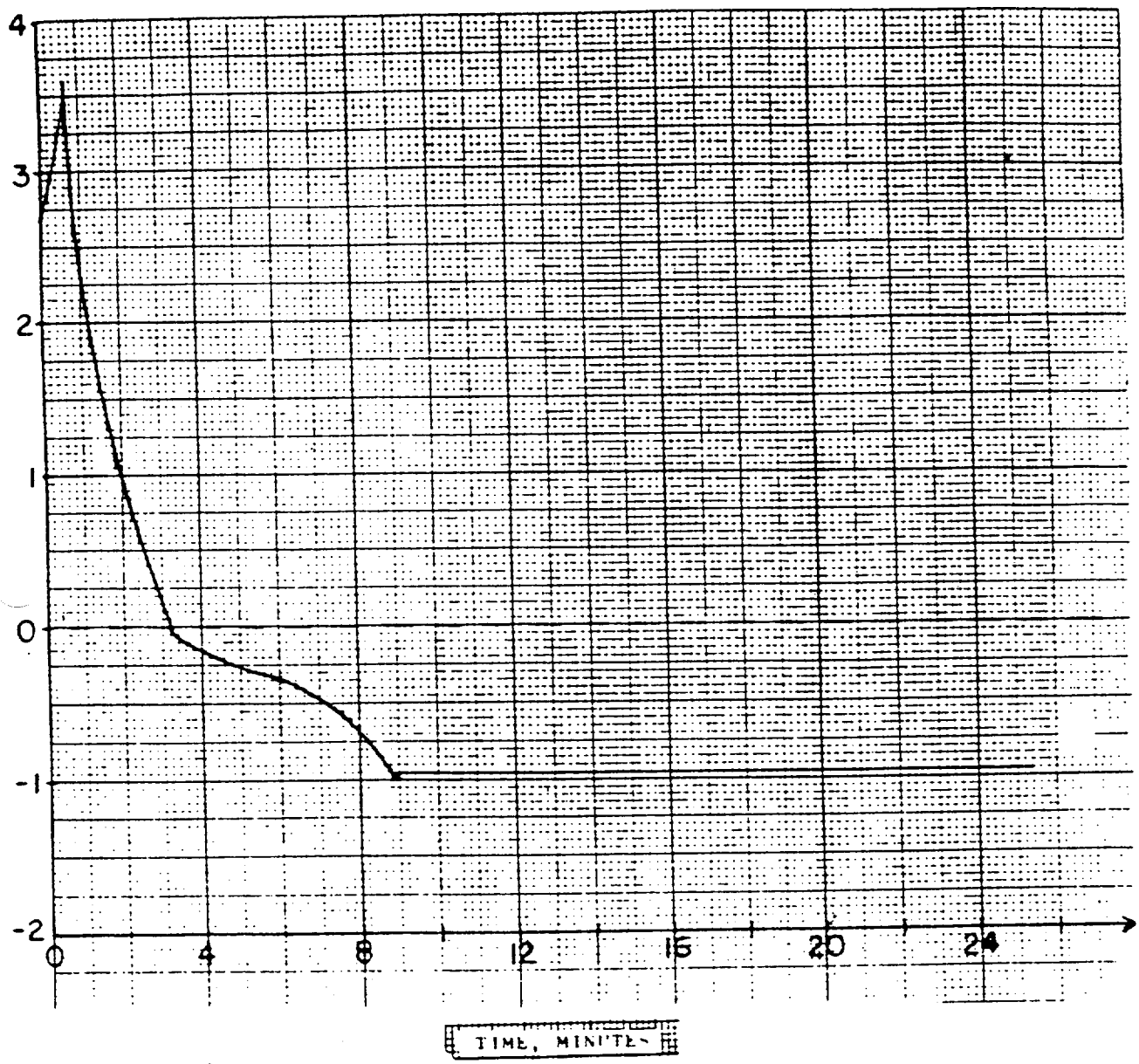

ANNULUS PRESSURE WITH STEEL SHELL
EXPANSION DESIGN BASIS TRANSIENT

FIGURE 14.3-37

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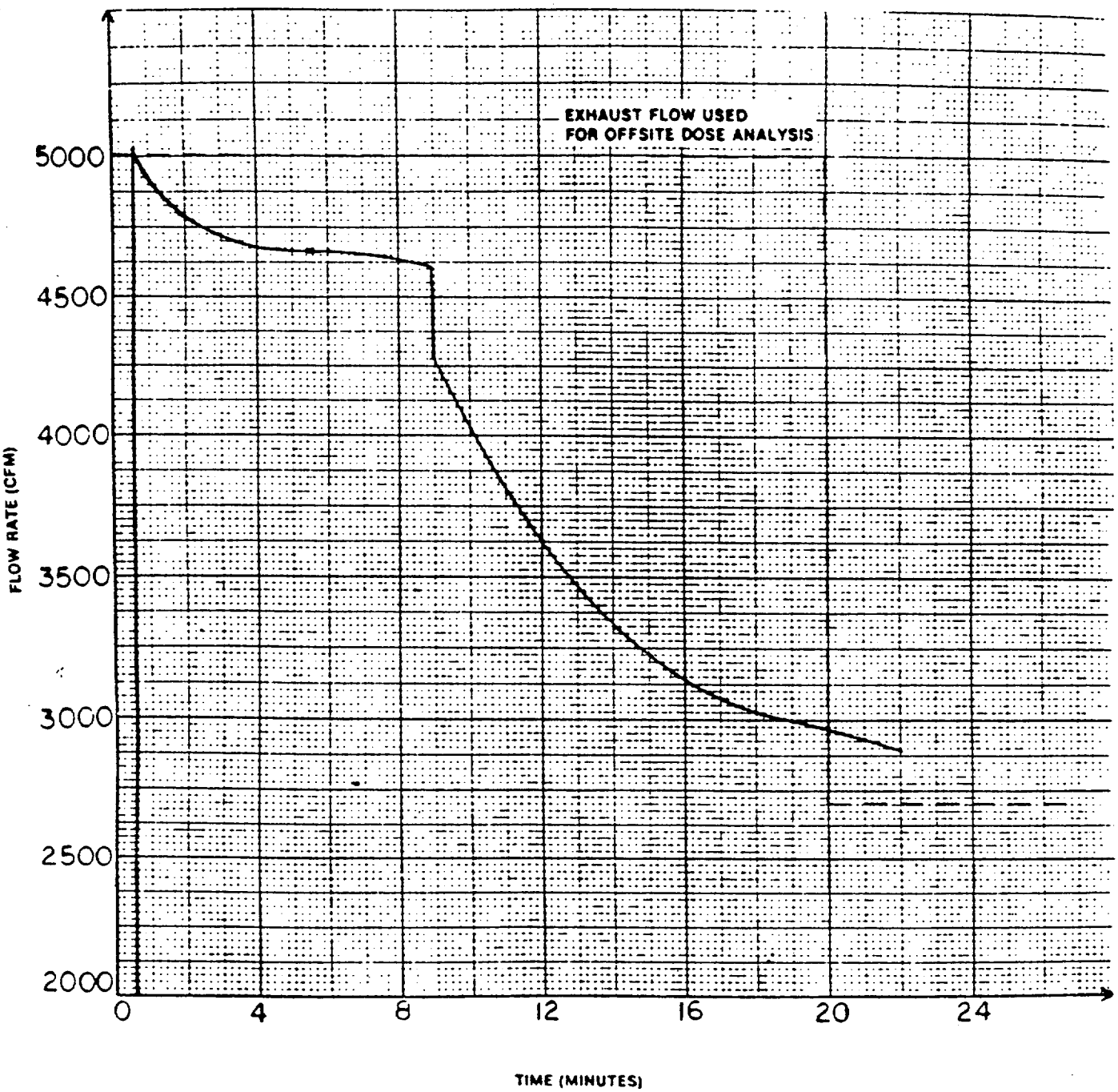

FLOW OUT OF THE SBVS WITH STEEL SHELL EXPANSION, DESIGN BASIS TRANSIENT

FIGURE 14.3-37A

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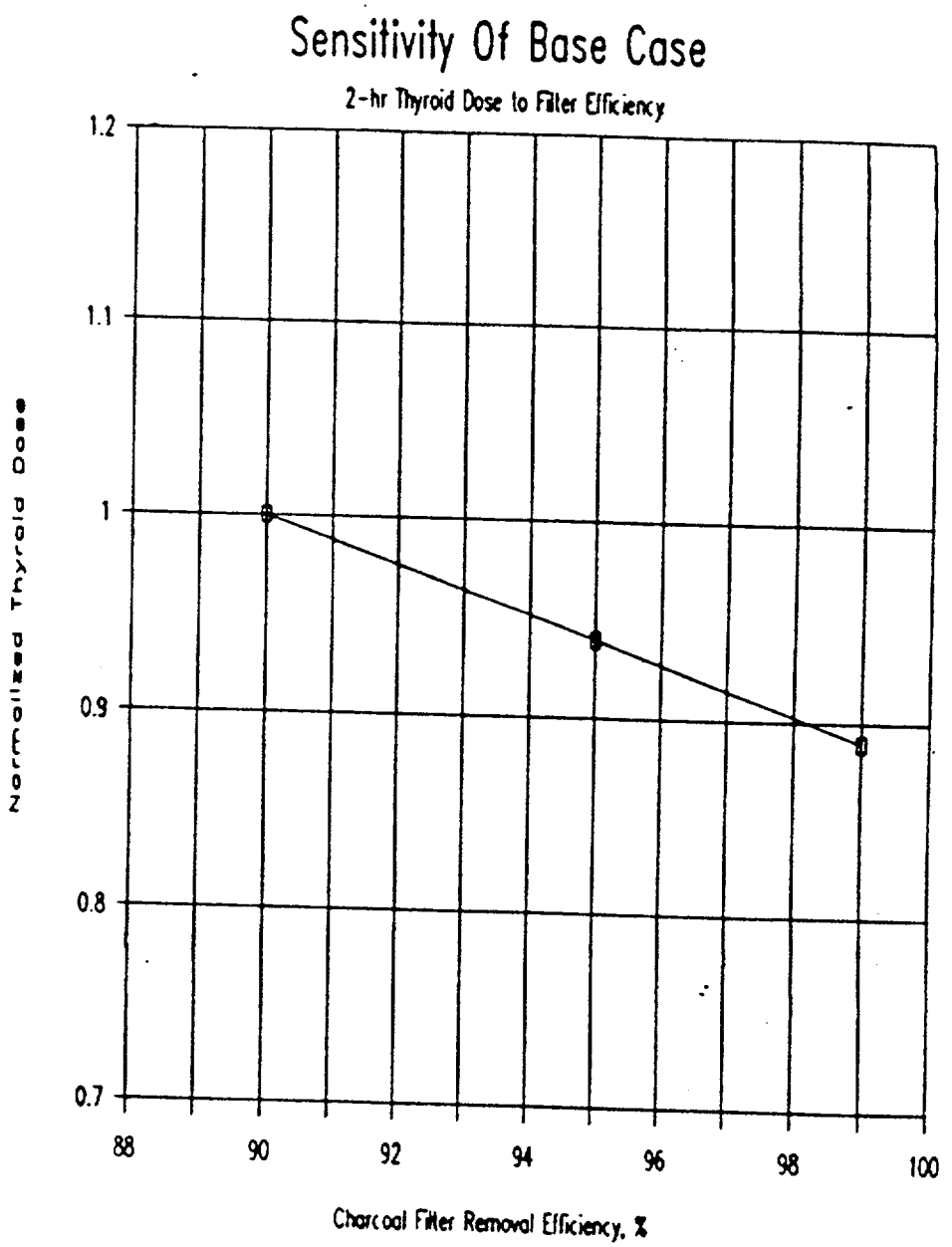

SENSITIVITY OF BASE CASE
TO FILTER EFFICIENCY

FIGURE 14.3-38

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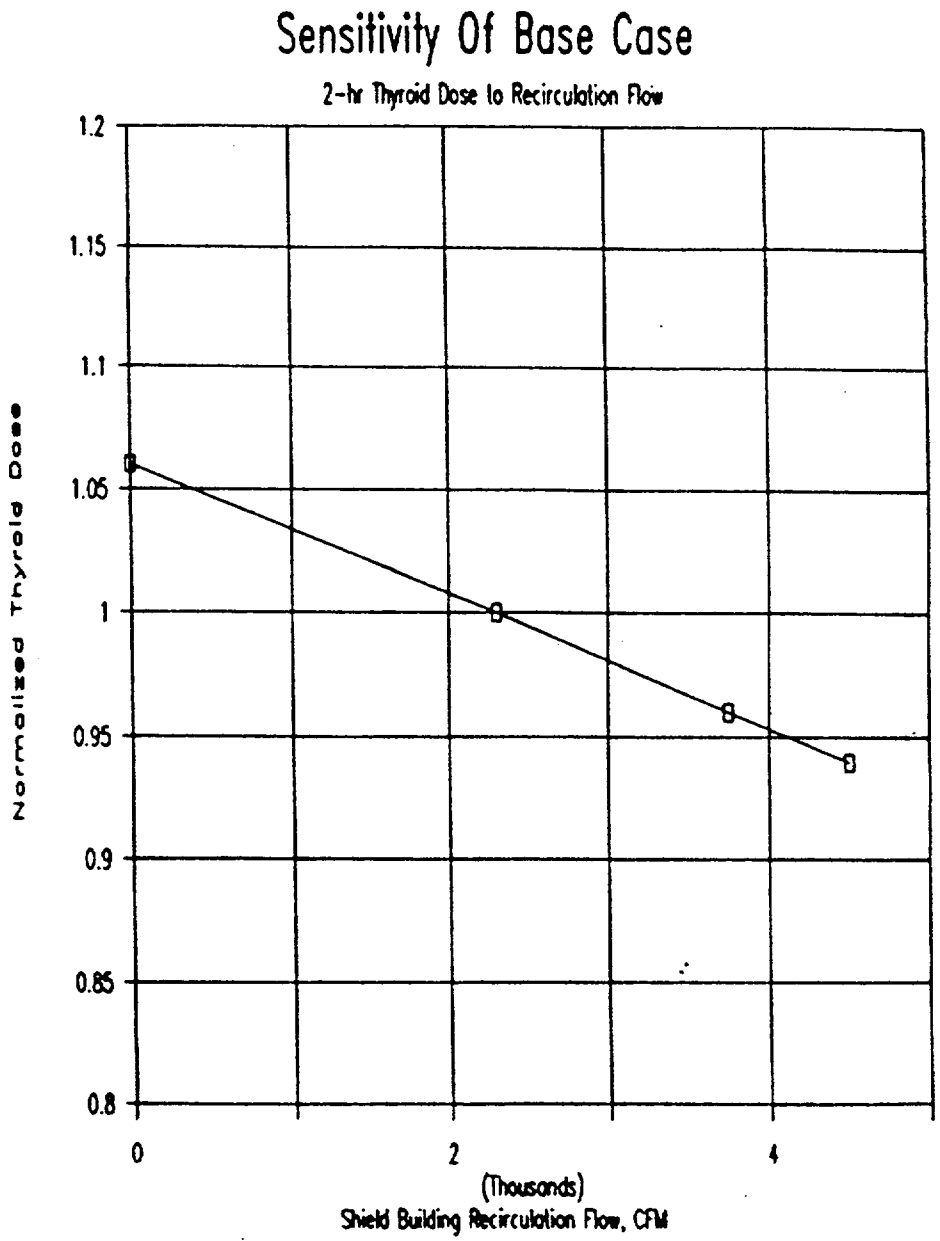

SENSITIVITY OF BASE CASE
TO RECIRCULATION FLOW

FIGURE 14.3-39

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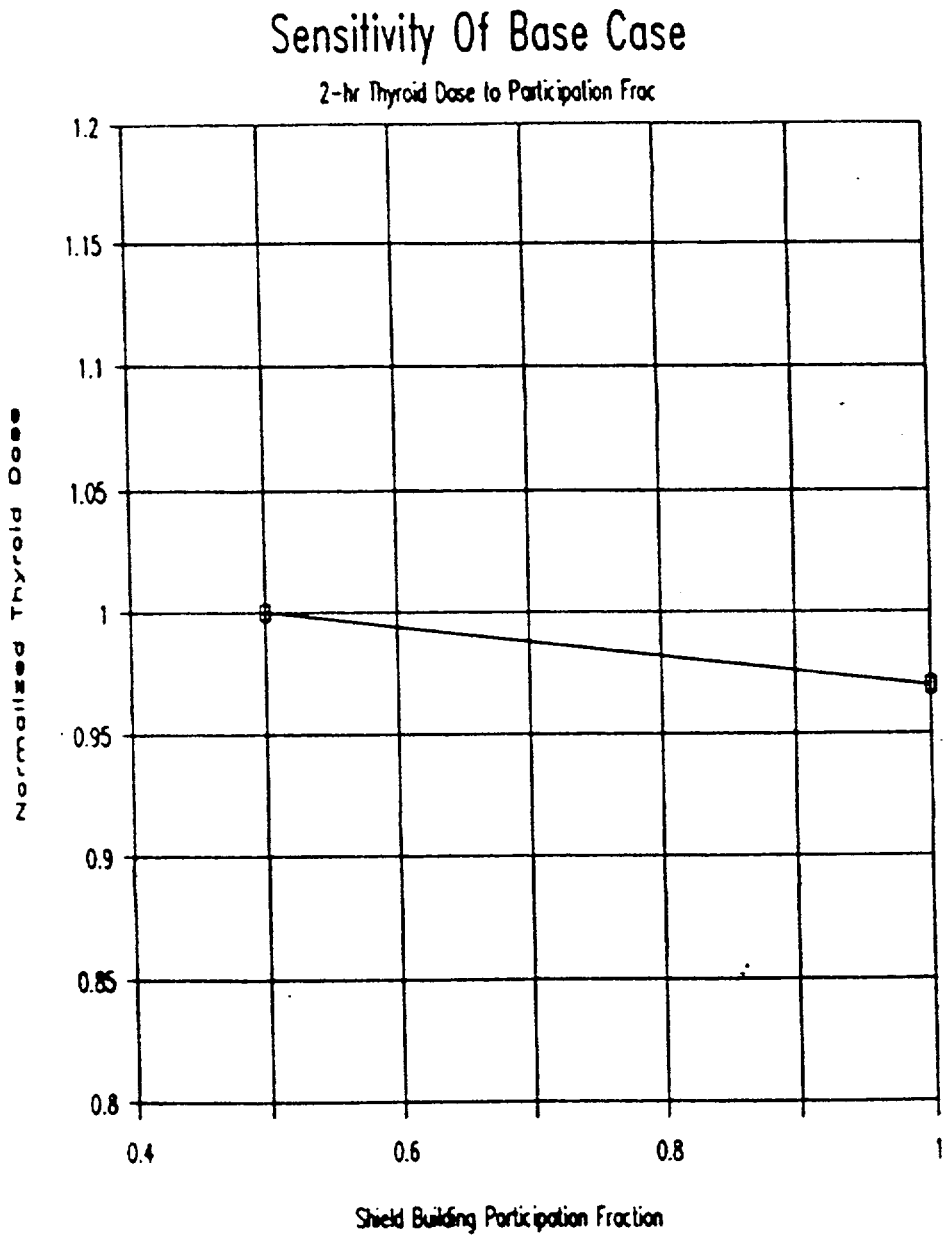

SENSITIVITY OF BASE CASE
TO PARTICIPATION FRACTION

FIGURE 14.3-40

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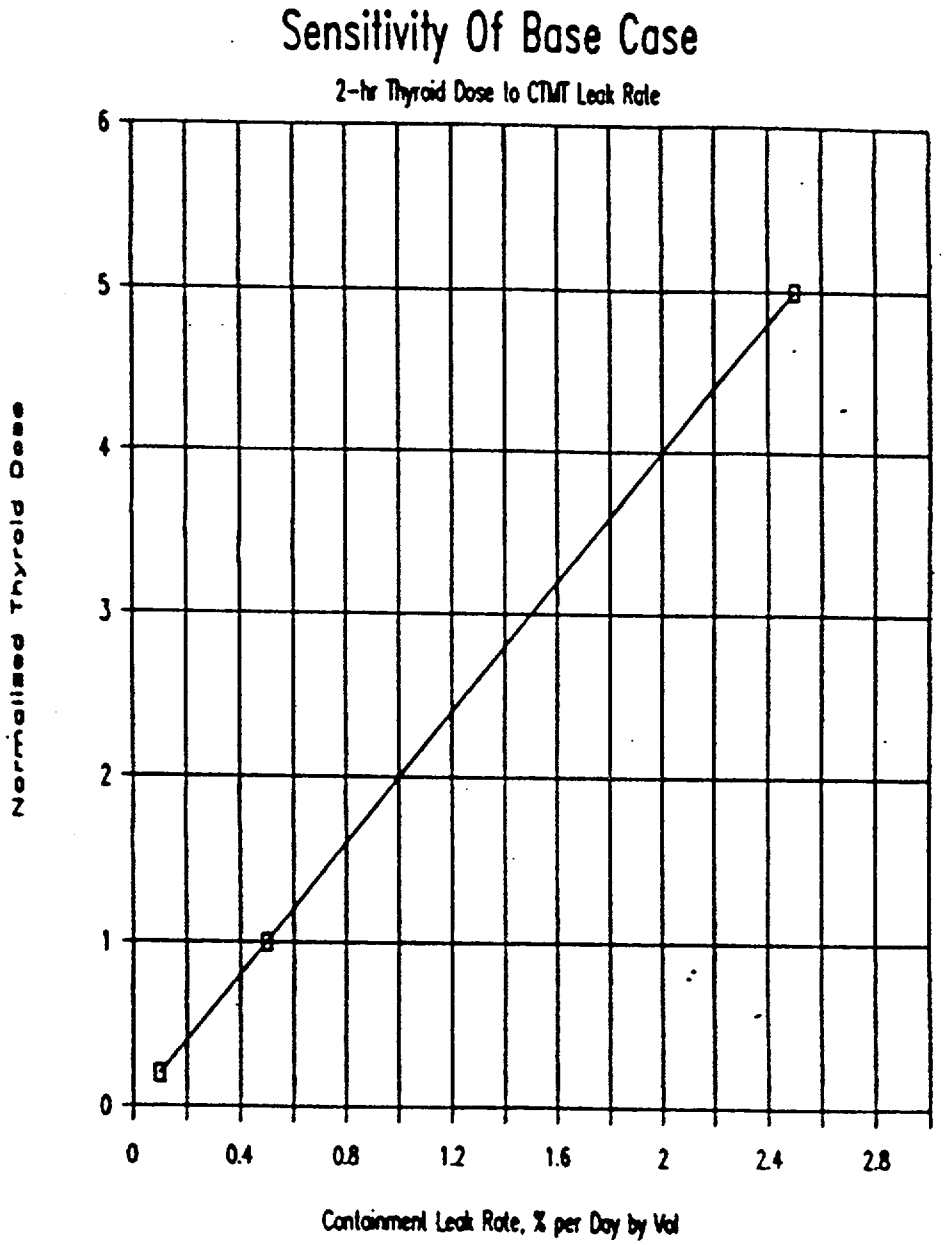

SENSITIVITY OF BASE CASE
TO CONTAINMENT LEAK RATE

FIGURE 14.3-41

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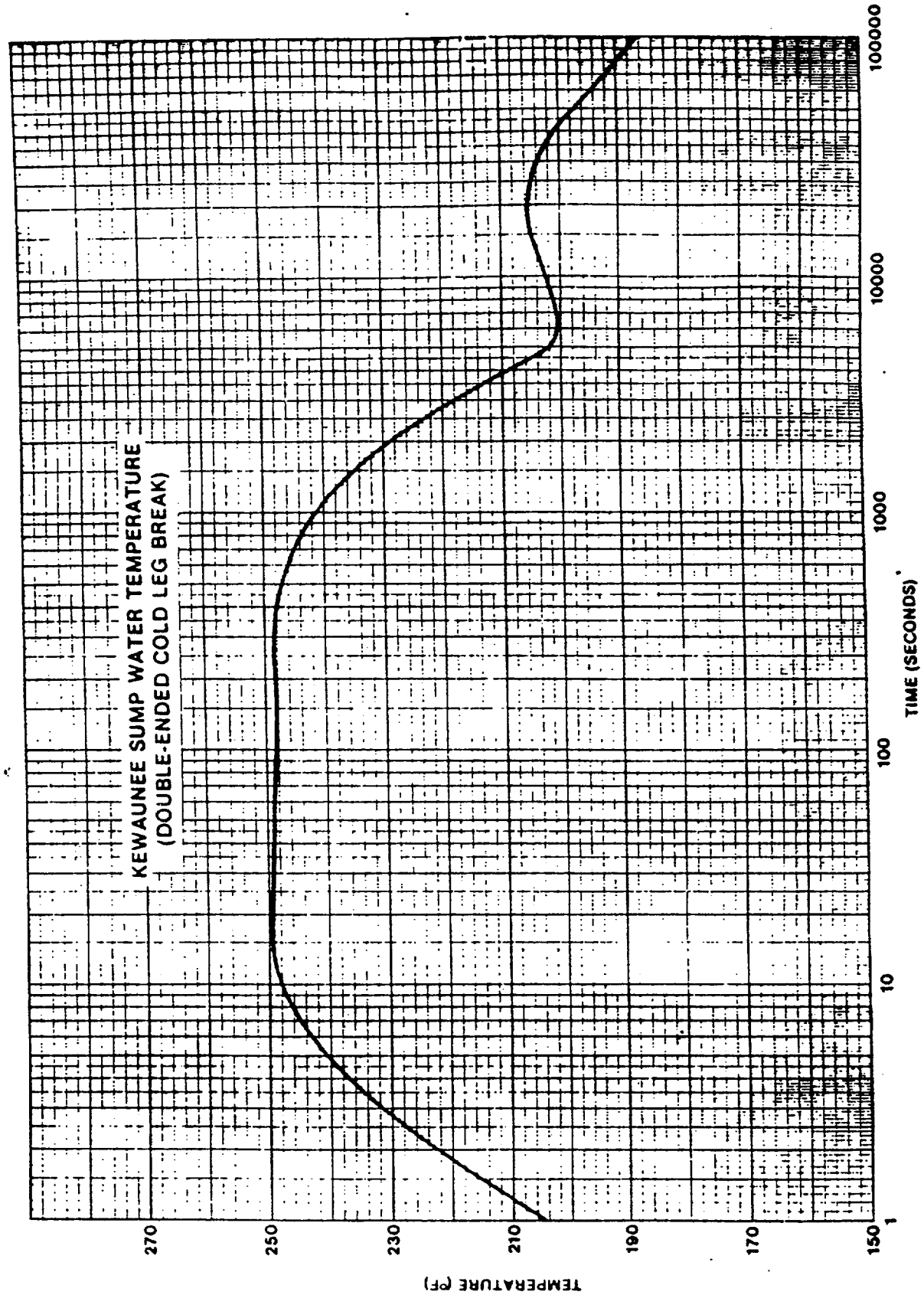

CONTAINMENT SUMP WATER TEMPERATURE TRANSIENT

FIGURE 14.3-42

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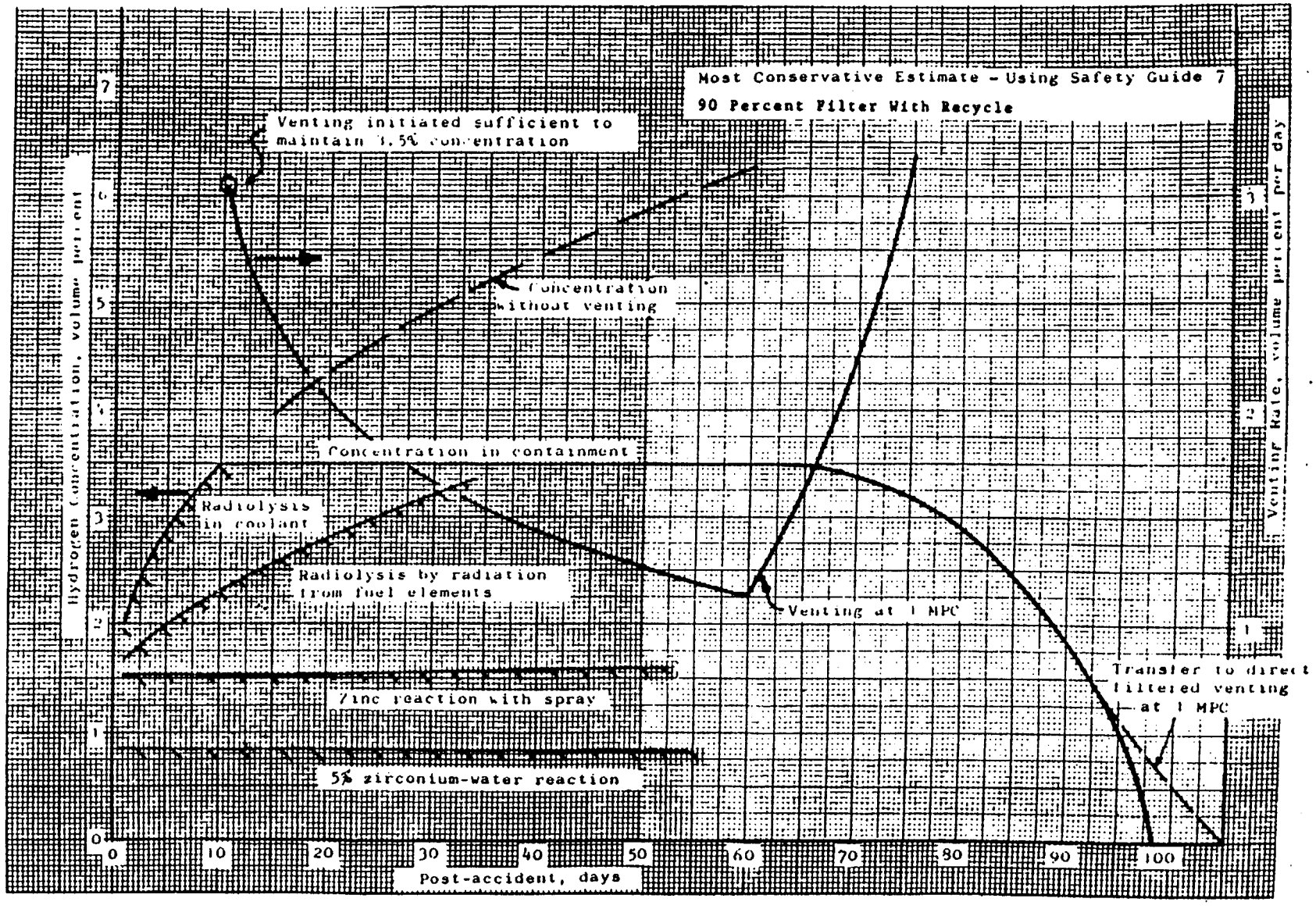

HYDROGEN CONTROL BY PURGING AND VENTING THROUGH SHIELD BUILDING VENT SYSTEM WITHOUT PRESSURE INCREASE

FIGURE 14.3-43

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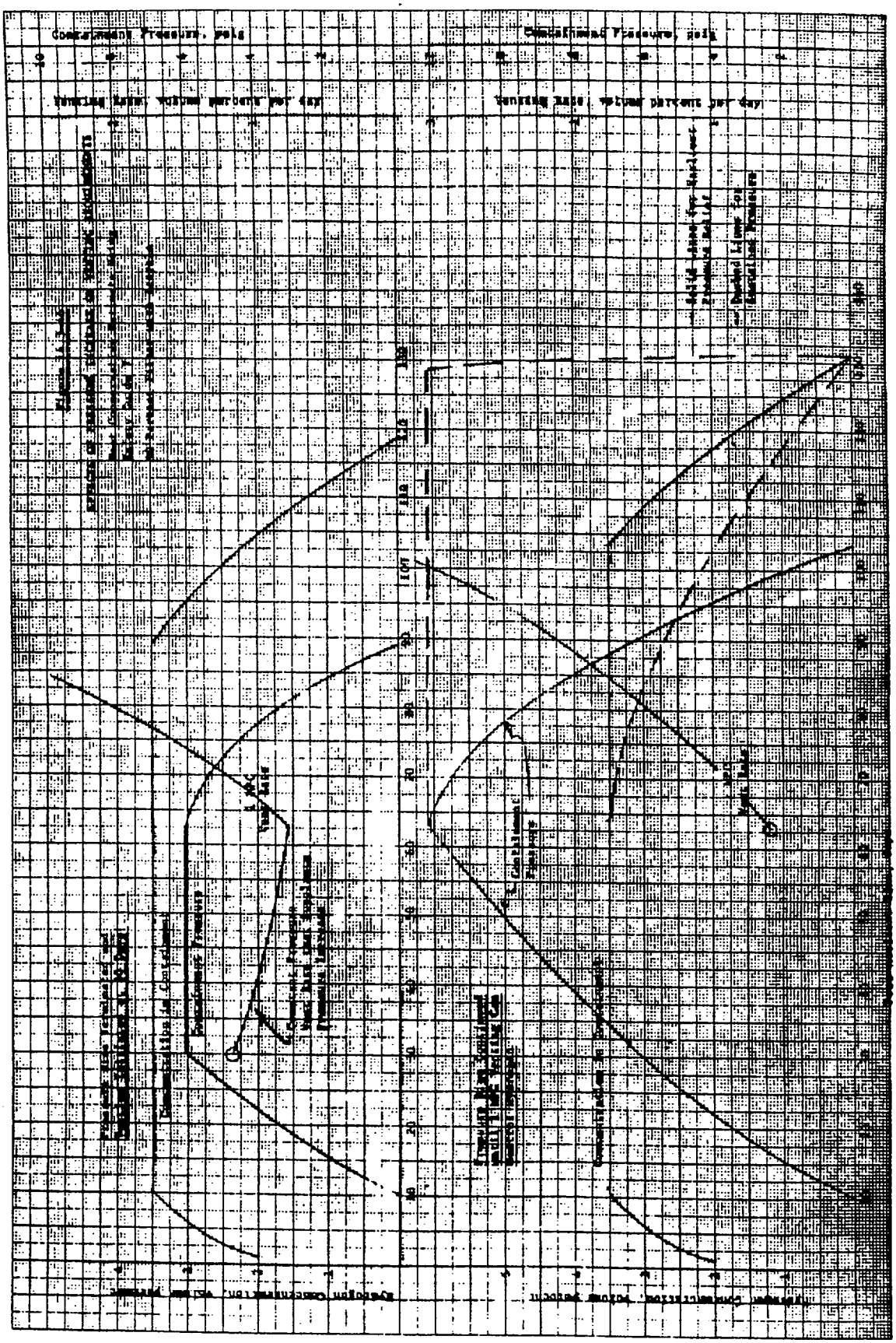

EFFECTS OF PRESSURE INCREASE ON VENTING REQUIREMENTS

FIGURE 14.3-44

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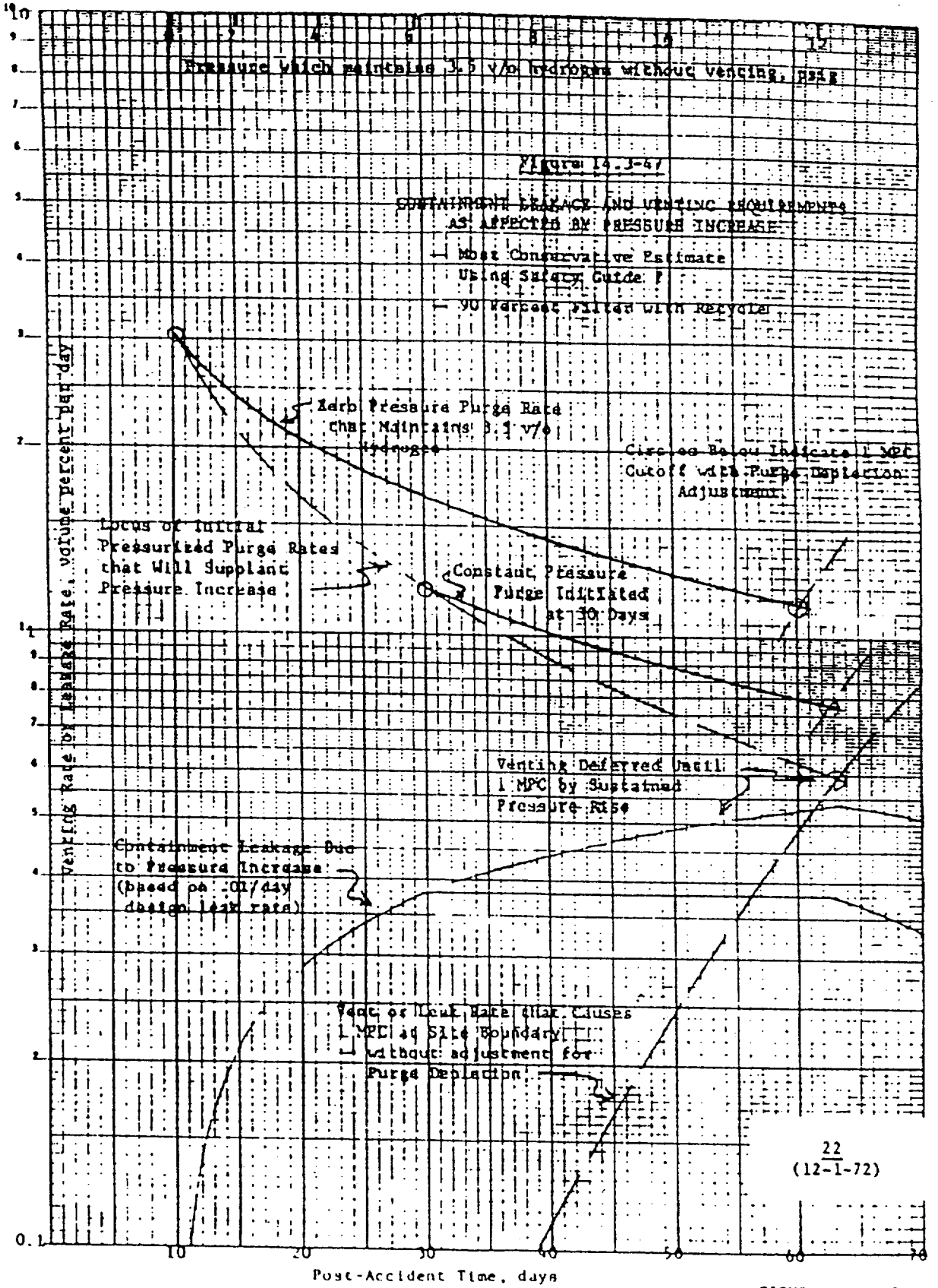

FIGURE 14.3-47

CONTAINMENT LEAKAGE AND VENTING REQUIREMENTS
AS AFFECTED BY PRESSURE INCREASE

FIGURE 14.3-45

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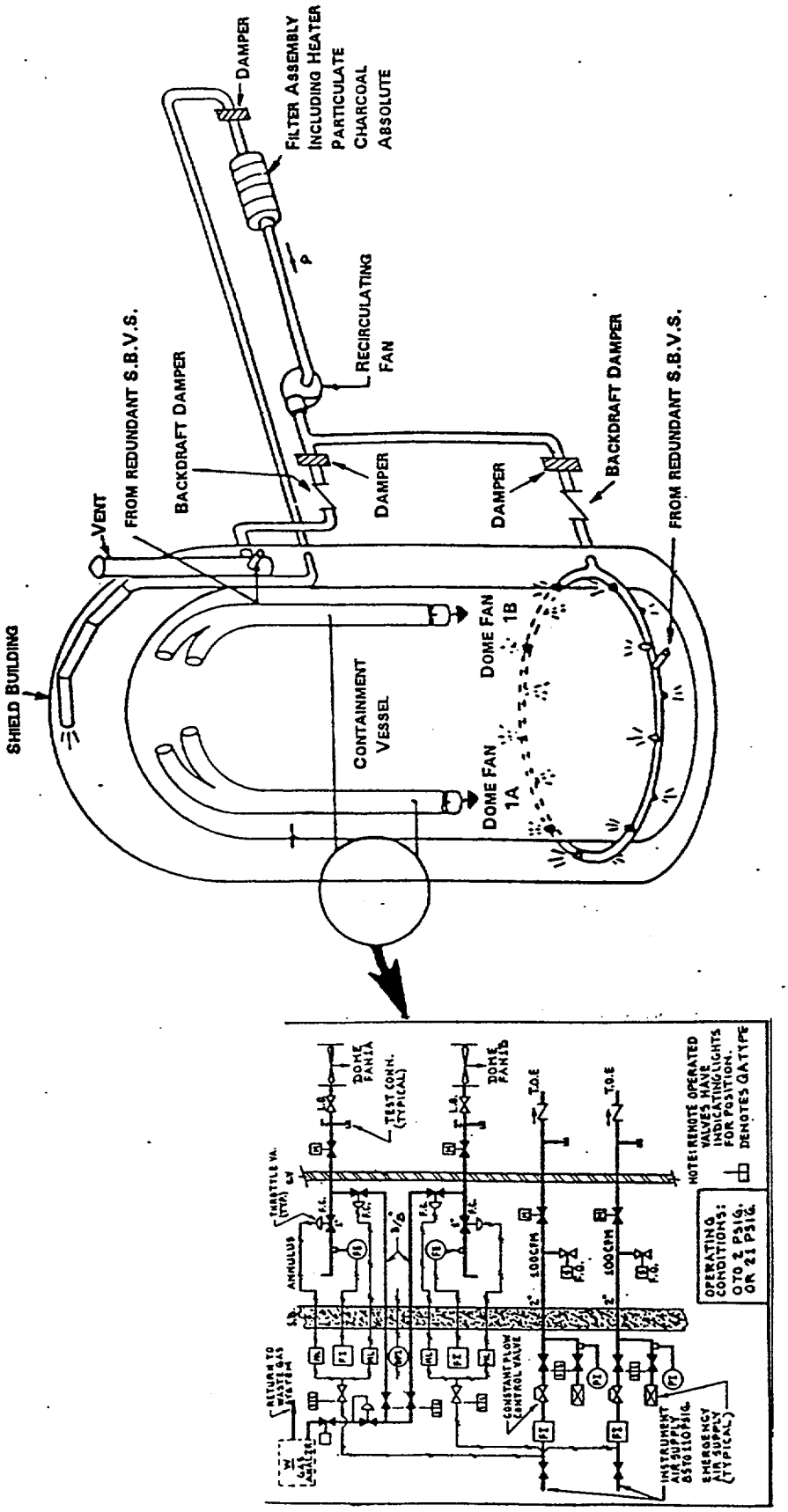

PROVISIONS FOR MIXING, SAMPLING AND VENTING OF CONTAINMENT GASES
FIGURE 14.3-46

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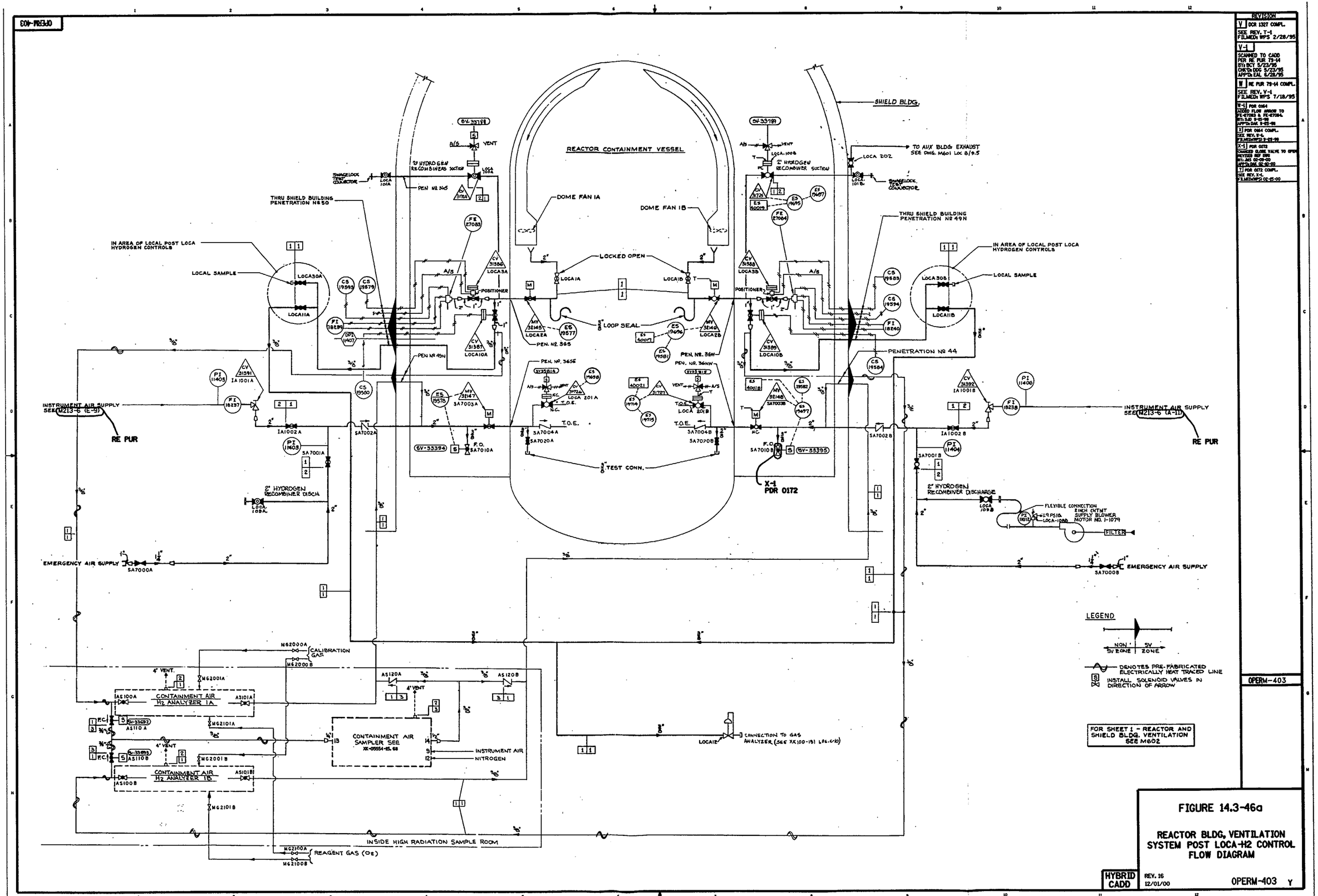

FIGURE 14.3-46a

REACTOR BLDG. VENTILATION SYSTEM POST LOCA-H2 CONTROL FLOW DIAGRAM

TABLE OF CONTENTS
APPENDICES 14A, 14B & 14C

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APPENDIX 14A

HEAT TRANSFER COEFFICIENTS USED IN LOCTA-R2

Heat Transfer Regime	Heat Transfer Correlation
1. Sub-cooled nucleate boiling	Jens and Lottes[1]

$$T_w - T_{sat} = 1.9\, e^{-P/900} \left(q''\right)^{1/4} \qquad (1)$$

2. Stable film boiling

X > 0.0 — Dougall and Rohsenow[2]

$$H = 0.023 \frac{k_v}{D_e} \left(\frac{\rho_v D_e}{\mu_v} \cdot \frac{Q_1 + Q_v}{A_c} \right)^{0.8} \left(\frac{C_p \mu}{k} \right)_v^{0.4} \qquad (2)$$

X < 0.0 — Sandberg et al.[3]

$$\left(\frac{hD_e}{k} \right)_f = 0.0193 \left(\frac{D_e G}{\mu} \right)_f^{0.80} \left(\frac{C_p \mu}{k} \right)_f^{1.23} \left(\frac{\rho_g}{\rho_b} \right)^{0.68} \left(\frac{\rho_g}{\rho_\ell} \right)^{0.068} \qquad (3)$$

3. Turbulent forced convection to steam — McEligot et al.[4,5]

$$\frac{hD_e}{k} = 0.020 \left[{}^{\mathrm{Re}}b \right]^{0.8} \left[{}^{\mathrm{Pr}}b \right]^{0.4} \left[\frac{T_w}{b} \right]^{-0.5} \qquad (4)$$

4. Laminar forced convection to steam — Hausen[6] and Kays[7]

$$\left(\frac{hD_e}{k} \right)_{iso} = 3.66\, v \qquad (5)$$

$$h \Big/ h_{iso} = \left(\frac{T_b}{T_w} \right)^{0.25}$$

5. Radiation to steam — Hottel[8]

$$q'' = \varepsilon \, x \, 0.1713 \left[\frac{(\frac{T_w}{100})^4 - (\frac{T_b}{100})^4}{T_w - T_b} \right] \qquad (6)$$

6. Pool film boiling — Bromley[9], Hsu and Westwater[10]

$$h = 0.7 \left(\frac{k_v 3 \, \rho_v \, (\rho L - \rho v) \, g h_{fg}}{L \quad (T_w - T_b) \, \mu_v} \right)^{0.25} \qquad (7)$$

7. Mist heat transfer in a dispersed flow — Dougall and Rohsenow[2]

$$h = 0.023 \frac{h_v}{D_e} \left[\frac{p_v \, D_e}{\mu_e} \quad \frac{Q_1 + Q_v}{A_c} \right]^{0.08} \left(\frac{C_\rho \mu}{k} \right)_v^{0.4} \qquad (8)$$

NOMENCLATURE SYMBOLS

A_c	=	Area of flow channel
C_p	=	Specific heat
D_e	=	Equivalent diameter
G	=	Mass velocity
L	=	Length of heat source
P	=	System pressure
Pr	=	Prandtl number
Q_l	=	Volumetric flow rate of liquid
Q_v	=	Volumetric flow rate of vapor
Re	=	Reynolds number
T	=	Temperature

g	=	Gravity constant
h	=	Heat transfer coefficient
h_{fg}	=	Latent heat of vaporization
k	=	Thermal conductivity
q"	=	Heat flux
X	=	Quality
e	=	Effective emmissivity
m	=	Dynamic viscosity
p	=	Density

SUBSCRIPTS

b	=	Quantities evaluated at bulk fluid temperature
f	=	Quantities evaluated at film temperature
g	=	Saturated vapor
iso	=	Evaluation of the parameter when the temperature difference (T_w - T_b) is small
l	=	Saturated liquid
sat	=	Refers to saturated condition
v	=	Saturated vapor
L	=	Refers to liquid
w	=	Wall

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APPENDIX 14B

WESTINGHOUSE HEAT TRANSFER RESEARCH AND DEVELOPMENT PROGRAMS

Several Westinghouse Research and Development Programs have resulted in confirmation of the usage of the above heat transfer correlations for loss-of-coolant analyses and also in the development of a transition boiling heat transfer correlation.

1. From the results of the Flashing Heat Transfer Program, the following conclusions were made:

2. Heat transfer in the transition to film boiling regime during the blowdown of a LOCA can be conservatively predicted by a correlation developed from steady-state film boiling and transition boiling data.

3. The McEligot, et al. correlation (References 4 and 5) realistically predicts the convective heat transfer coefficients in turbulent flow.

4. In turbulent flow, the radiant heat transfer contributions to the total heat transfer coefficient is adequately predicted by Hottel's (Reference 8) technique.

5. In laminar flow the total heat transfer coefficient is conservatively predicted by using the correlation of Hausen (Reference 6) and Kays (Reference 7) for the convective contribution and the method of Hottel (Reference 8) for the radiant contribution.

In progress at the time of this application was the Full-length Emergency Cooling Heat Transfer (FLECHT) Program. The objective was to investigate the behavior of a simulated pressurized water reactor during the core recovery (re-flooding) period, which follows a loss-of-coolant accident. The Group I and Group II test series of this program, which included 7 x 7 and 10 x 10 rod bundle arrays, have been completed.

Transient heat transfer coefficients and clad temperature behavior, at different radial test bundle locations and at various axial locations, were investigated over the following range of parameters:

♦ Peak Power Density	0.69 to 1.40 kw/ft
♦ Maximum Initial Clad Temperature	800 to 2200°F
♦ Flooding Rate	0.6 to 18 in/sec
♦ Inlet Sub-cooling	16 to 189°F
♦ System Pressure	15 to 90 psia

In addition to the effects of the above parameters, the effects of borated coolant and simulated flow blockage due to clad swelling were also investigated.

In summary, the current test results verified the ability of a bottom flooding emergency core cooling system design to terminate the temperature increase during a loss-of-coolant accident. In particular, it

has been shown that the effects of a variable flooding rate can be predicted using constant flooding rate data and that complete blockage of as many as sixteen adjacent channels will not impair bottom flooding emergency core cooling effectiveness.

Heat transfer coefficients obtained from the FLECHT Program have been used in the loss-of-coolant accident evaluation of this reactor.

APPENDIX 14C

CONTAINMENT PRESSURE RESPONSE TO LOCA

14C.1 STEAM GENERATORS AS AN ACTIVE HEAT SOURCE

The containment pressure response has been analyzed considering the steam generators as an active heat source during re-flood. The analysis presented is for the 3.0 ft^2 pump suction break, which has been found to be most conservative. In addition, sensitivity studies are presented to show the containment pressure transient as a function of break size and location.

The calculational model may be divided into three parts: Blowdown, when the system pressure drops from 2250 psia to containment pressure; Refill, when the vessel inventory is increased to the bottom of the core; and Re-flood, where the water level moves into the core.

BLOWDOWN - The model for blowdown is essentially the same as that used in the FSAR containment analysis. The SATAN code is used to simulate breaks in the various locations. All accumulators inject for breaks other than the cold leg.

The steam generator is modeled using several well-known heat transfer correlations. When the heat flow in the steam generators is from primary to secondary, the heat transfer coefficient on the tube side is calculated using the Dittus Boelter (Reference 11) correlation for sub-cooled forced convection, while the shell side uses the well known Jens-Lottes (Reference 1) correlation for nucleate boiling. For secondary to primary heat flow, the tube side heat transfer coefficient is calculated using the Jens-Lottes correlation for nucleate boiling. This calculation will be bypassed if the tubes experience Departure from Nucleate Boiling (DNB). The DNB ratio is calculated using Macbeth's (Reference 12) correlation of the critical heat flux. When the value of this ratio drops below an input value, the Dougall-Rohsenow (Reference 2) film boiling correlation is used. Should the fluid in the steam generator tubes become superheated, the superheat forced convection correlation developed by McEligot (Reference 4) is used. The shell side heat transfer coefficient when the heat flow is from secondary to primary, is the only difference in the steam generator model used in the FSAR and the present model.

Previously, this value was maintained at the high initial value, thus allowing an exceedingly high heat flow from the secondary to the primary side of the steam generators. In the present model the heat transfer coefficient on the shell side when heat flow is from secondary to primary is calculated using McAdam's (Reference 13) recommended correlation for turbulent boundary layers on vertical surfaces.

Table 14C-1 lists all of the heat transfer correlations referenced formerly. The heat flow rates vs. time curves for the broken loop steam generator and the intact loop steam generator are presented in Figures 14C-1 and 14C-2 for the worst case (3-ft^2 pump suction break).

The fluid volume contained in the primary system has been adjusted slightly. This volume reflects the correct system volume, calculated from component dimensions, plus 1.6% to account

for thermal expansion and 1.4% to account for uncertainties. The initial fluid energy presented in the FSAR was based on a fluid volume in excess of that calculated using the standard procedure described above.

The initial fluid energy is also based on coolant temperatures, which are the maximum levels attained in steady-state operation including allowance for instrument error and deadband.

The previous SATAN initial stored energy in the core has been identified as being exceedingly conservative. The stored energy has been evaluated using a detailed temperature model of the pellet, clad and gap. The temperature distribution within the fuel pellet is predominantly a function of the local power density and the UO_2 thermal conductivity. However, the computation of radial fuel temperature distributions combines crud, oxide, clad, gap and pellet conductances. The factors which influence these conductances, such as gap size (or contact pressure), internal gas pressure, gas composition, pellet density, and radial power distribution within the pellet, etc., have been combined into a semi-empirical thermal model. This thermal model has been incorporated into a computer code to enable the determination of these factors and their net effects on temperature profiles. The temperature predictions of the code have been compared to in-pile fuel temperature measurements and melt radius data with good results. Table 14C-2 presents the results of sensitivity study on core stored energy, in full power seconds above average coolant temperature, varying the following parameters:

- Average Power Level
- Number of nodes assumed in the pellet
- Effect of fuel densification

A conservative value of 7.9 (6.6 x 1.2) full power seconds, which includes fuel densification and additional margin, was used in this analysis. Moreover, core stored energy was based on a conservative value of 102% of the engineered safeguards design rating power level.

Table 14C-3 summarizes the core stored energy plus core power generation (during blowdown) used in this analysis. The margins cited above clearly indicate that the values employed in this analysis represent a conservative upper bound of the core-stored energy.

The amount of heat released from the core over blowdown has been studied and an upper bound has been determined by a suitably conservative analysis. The SATAN code heat release predictions were compared to those of an average channel analysis using the LOCTA code.

The LOCTA code, for calculation of heat release, was modified to obtain a conservatively high value. Coefficients in the transition boiling correlation were modified to obtain a realistic fit of the approximately 500 data points. An informal submittal (proprietary) was made to the NRC in November 1971.

The DNB time was increased to 2.5 seconds. The modified transition boiling correlation and the larger DNB time are in the direction of a conservative heat release for containment pressure. Separate analyses provide for simulation of more detailed effects in the pellet temperature calculation, and thus, in the core energy release calculation. Moreover, the

incremental energy associated with the different schemes is small compared to total blowdown release.

The difference between the LOCTA heat release and the SATAN heat release is added directly to the blowdown energy rate transient by the following method:

$$(mh)_{\substack{to \\ containment}} = (mh)_{\substack{SATAN \\ Blowdown}} + m_{\substack{SATAN \\ Blowdown}} \times \bar{h}$$

where: $$\bar{h} = \frac{Q_{LOCTA} - Q_{SATAN}}{M_{Blowdown}}$$

where:

mh	=	Enthalpy flow rate
m	=	Mass flow rate predicted by SATAN
$\bar{h}$	=	Incremental blowdown enthalpy
Q_{LOCTA}	=	LOCTA core heat release
Q_{SATAN}	=	SATAN core heat release
$M_{Blowdown}$	=	Mass released over blowdown

In summary, the following items insure that the energy release is conservatively analyzed for maximum containment pressure.

- Maximum expected operating temperature
- Allowance in temperature for instrument error and dead band (+4°F)
- Margin in volume (1.4%)
- Allowance in volume for thermal expansion (1.6%)
- Margin in core power associated with use of engineered safeguards design rating (ESDR)
- Allowance for calorimetric error (2% of ESDR)
- Conservatively modified coefficients of heat transfer
- Allowance in core stored energy for effect of fuel densification
- Margin in core stored energy (+20%)

REFILL - The calculations in this period have been minimized by making the conservative assumption that the bottom of core recovery occurs immediately after the end of blowdown.

Description of the Core Re-flooding Model

The SATAN calculations are performed until the completion of blowdown. In this context the end of blowdown is defined as the time at which zero break flow is first computed. At this time the normal blowdown transient calculations are terminated and the re-flooding calculations are performed.

The re-flooding calculations are done in the following two steps:

1. Calculate the core inlet mass flow rate and the fraction of the inlet mass flow rate that leaves the top of the core. This hydraulic calculation yields core flooding rate and entrainment fraction.

2. Calculate the core exit conditions due to the addition of various energy sources. Also perform calculations of the thermal conditions on the primary and secondary side of the steam generator. This step is an energy balance calculation.

Hydraulic Model

The hydraulic model represents the downcomer region and the active core region with two volumes. The core communicates with the downcomer via a non-resistive flow path representing the lower plenum region. The core and the downcomer communicate with the containment via flow paths with resistances, which represent the broken loop and the intact loop. The model is shown in Figure 14C-3. The resistances used to calculate the loop pressure drop are based on a loop area of 4.5 square feet. Standard losses from Crane (Reference 14) were used to model area changes and losses due to fittings. Frictional losses were calculated assuming typical re-flood conditions, and then calculating friction factors based on homogeneous fluid assumptions. The losses in the core and the hot leg were increased by 30% to allow for the effect of higher-pressure drops for two-phase flow conditions. The components of the loop losses as illustrated in Figure 14C-3 are summarized in Table 14C-4.

The core-flooding rate is limited by the pressure in the core caused by the generation of steam when the re-flood water is heated up by the hot fuel rods. Any steam generated in the core region must be vented through the intact and broken loops via the resistive paths shown in Figure 14C-3.

Steam, which flows through the intact steam generator must encounter the injected water in the cold legs of the broken and intact loops. During the accumulator injection phase an equilibrium calculation indicates that the amount of water available is sufficient to condense this steam, thus reducing the flow to the containment. Moreover, preliminary results from steam-water mixing experiments performed by Westinghouse indicate that the heat transfer between the steam and injected liquid is quite high, and justify an equilibrium calculation. This benefit was not included in the present calculations.

The steam-water experiments also indicate an increase in resistance in this section of pipe over the steam flow only resistance considered in the present analysis. This increase in resistance would provide some reduction in flooding rate, an effect which is conservatively omitted in the present analysis.

The pressure drops along the two paths in these calculations were calculated with the associated loss coefficients and with fluid density conservatively evaluated at maximum core pressure during the hydraulic transient. In actuality, the fluid density varies around the loop, becoming smaller as the local pressure decreases from core pressure to containment pressure. Again, the

reduction in flooding rate due to this effect has not been included in the present analysis. This phenomenon results in less flow for a given pressure drop and hence reduced energy flow to the containment. The pressure drop across the pump is calculated by assuming that the rotor is free spinning. However, a sensitivity study was performed to compare free spinning assumptions to locked assumptions on the rotor for calculation of pressure drop across the pump. This study showed a slightly higher flooding rate for the locked rotor case with an associated increase in energy release over re-flood of about 0.6E+6 Btu. The increased flooding rate for locked rotor assumption vis-a-vis the free spinning assumption is true to the pump suction and not the cold-leg break. The core exit quality in the hydraulic calculation is based on lower value of heat flux as compared to the total contribution of remaining core stored energy, hot metal energy, and decay heat. This results in a larger value of core exit density and hence a slightly (about 7%) higher flooding rate. This is conservative for containment pressure calculations. The fraction of calculated core flooding rates that is vaporized and entrained is calculated using the Westinghouse entrainment correlation obtained from the FLECHT results. The resulting transient values of core flooding rate and the entrainment fraction is utilized in the energy balance model to calculate mass and energy release rates to the containment for calculation of the containment pressure transient.

Energy Balance Model

The energy balance model consists of three reference elements which represent the core, the steam generator in the broken loop, and the steam generator in the intact loop. Figure 14C-4 presents a diagram of the model where the variables shown are defined as follows:

m	=	Mass flow rate into the core (lb.m/sec)
$(mh)_{in}$	=	Energy flow rate into the core (Btu/sec)
$(mh)_{exit}$	=	Energy flow rate out of the core (Btu/sec)
m_1	=	Mass flow rate to the broken loop steam generator (lb.m/sec)
m_2	=	Mass flow rate to the intact loop steam generator (lb.m/sec)
$m_1 h_{out_1}$	=	Energy flow rate from broken loop steam generator out into containment (Btu/sec)
$m_2 h_{out_2}$	=	Energy flow rate from intact loop steam generator out into containment (Btu/sec)
q_{heat}	=	Sum of heat sources to the core fluid (Btu/sec)
h_f	=	Saturated liquid enthalpy (Btu/lb.m)
q_{SG1}	=	Heat flow rate from the broken loop steam generator (Btu/sec)
q_{SG2}	=	Heat flow rate from the intact loop steam generator (Btu/sec)

An energy balance is performed on the fluid entering and leaving the core in order to determine core exit conditions:

$$(mh)_{in} + q_{heat} = (mh)_{exit} + (m_{in} - m_{exit})h_f$$

The mass flow rate of fluid entering the core is identical to the calculated flooding rate times the product of the core area and liquid density. This fluid is taken to be at injection conditions. The heat source term is added to the fluid in the core and is the sum of the following:

- Decay heat
- Thick metal (reactor vessel) heat
- Core stored energy left at end of blowdown
- Thin metal energy remaining at end of blowdown

Decay heat is calculated using the Westinghouse standard decay heat curve and evaluated at 102% of Engineered Safeguards Design Rating. The core stored and thin metal energy, that are remaining at end of blowdown, are brought out at a constant rate over the period between the bottom of core recovery (end of blowdown) and the quench of the 8-foot elevation in the core. The thick metal decays exponentially with a time constant of 0.0032^{-1} seconds.

This treatment of energy release may result in core exit temperatures, which are higher than the steam generator temperature for the period where the exit mass flow is small. However, the effect on containment pressure is small because of the short duration of this small core exit mass flow (about two seconds). In particular, for the present analysis the amount of energy transferred to the steam generators was 0.2E+6 Btu for the double-ended pump suction break and 0.05E+6 Btu for the 3-ft^2 break. This energy is not lost but increases the steam generator internal energy and is available to flow back into the containment.

The mass flow rate leaving the core is equal to the inlet flow rate times the entrainment fraction calculated from the hydraulic model. The difference between inlet and outlet flow represents the fluid, which remains in the core and this is heated to saturated liquid enthalpy.

The above considerations provide sufficient information to determine the core exit enthalpy.

The flow split between the intact loop and the broken loop steam generators is determined in the hydraulic model described earlier. Separate energy balances are performed on the broken loop and intact loop steam generators. Fluid, which enters the primary side of the steam generator is assumed to be heated instantaneously to the shell side temperature. This sets the outlet enthalpy; the steam generator inlet enthalpy is equal to the core exit enthalpy. Hence, the energy addition from the steam generators to the fluid entering the containment is determined.

This energy flow results in a decrease in internal energy for the shell side of the steam generator. Metal heat on the secondary side is included in the internal energy calculation. The steam generator secondary side fluid mass (and hence, density) is taken as constant and temperature can be found directly from the internal energy. Feedwater addition is not considered in the present analysis; this effect would reduce steam temperature, hence, omission is conservative.

The fluid which leaves the steam generator primary side is assumed to flow directly into the containment. No credit is taken for the quenching effect of the accumulator water, which spills to containment.

For hot leg breaks the flow that leaves the core is subdivided between the direct flow to the containment and that to the intact loop, vessel annulus and broken loop path. Since about 85% of the flow goes directly into the containment, not much energy is extracted from the steam generators and a lower peak containment pressure results. For cold leg breaks, the flooding rates are lower than for the pump suction breaks because of the resistance of the pump in the broken loop. This results in less energy release during re-flood. The pump suction breaks are worse than either the hot or cold leg breaks.

Results

The results of the analysis described above shows that the 3-ft^2 pump suction break yields the maximum containment pressure. The mass and energy released to the containment as a function of time for this break are given in Figures 14C-5 and 14C-6.

For this case the accumulator flow rates to the intact loop and broken loop are given in Figures 14C-7 and 14C-8, also the core inlet velocity as a function of time after bottom of core recovery is given in Figure 14C-9. This results in the containment pressure transient given in Figure 14C-10. The peak pressure for this design case is 43.1 psig.

In addition to the above case, the following cases were considered to determine the sensitivity of the pressure transient to break size and break location. These calculations demonstrate that the worst break is the 3.0-ft^2 pump suction break.

- Figure 14C-11 gives the Containment Pressure Transient for the Double-Ended Guillotine Pump Suction Break (10.5 ft^2).
- Figure 14C-12 is for the 0.6 Double-Ended Guillotine Pump Suction Break (6.3 ft^2).
- Figure 14C-13 is for the 4.0-ft^2 Pump Suction Break.
- Figure 14C-14 is for the 2.0-ft^2 Pump Suction Break.
- Figure 14C-15 is the Double-Ended Guillotine Cold Leg Break (8.25 ft^2).
- Figure 14C-16 is for the Double-Ended Guillotine Hot Leg Break (9.2 ft^2).
- Figure 14C-17 is for the 0.6 Double-Ended (5.5-ft^2) Guillotine Hot Leg Break.

The blowdown enthalpy, length of blowdown, the blowdown peak pressure and the peak containment pressure for the various cases analyzed are summarized in Table 14C-5.

The contribution of core stored energy, decay heat, thick-and thin-metal heat, and steam generator energy to total re-flood enthalpy for the various cases analyzed are summarized in Table 14C-6.

In addition to the above, the following analyses have been performed to further verify that the peak calculated pressure following a LOCA will not exceed the design pressure.

Case 1: The 3.0 ft2 pump suction break has been analyzed with the following assumptions:

- Entrainment during re-flood continues until the quench front reaches the 8-foot level.
- Water in the lower plenum is initially saturated with temperature decreasing during the re-flooding transient to 230°F.
- The core-flooding rate has been analyzed using a more detailed pressure drop calculation and local densities throughout the loop.
- The structural heat sinks given in Table 14C-7 have been used, and these replace the values given in Table 14.3-4, which were used in previous containment pressure calculations. The values in Table 14C-7 represent the as-built configuration inside containment.

This case resulted in a peak containment pressure of 39.2 psig.

Case 2: The 3.0-ft^2 sump suction break has been further analyzed showing a peak pressure of 40.5 psig. The assumptions in this case were the same as those in Case 1 above except that the entrainment during re-flood was arbitrarily extended until the quench front reached the 10-foot level to show additional margin. The mass and energy releases for this case are given in Figure 14C-18.

Additional Post-Re-flood Energy Release Analysis

Consideration is given here to the possible additional energy release to containment during the post re-flood phase of the large break accident. This postulated additional energy would result from the presence of a two-phase mixture in the steam generator tubes. A proper analysis of this phenomena requires a simulation of the RCS behavior that cannot be done at the present time.

FLECHT - SET, PHASE B. will address the post re-flood and re-flood phases of the LOCA. It will determine the two-phase mixture level and the core and steam generator heat release during the post re-flood transient. These results are expected to confirm the conservatism associated with the present containment energy release calculation.

However, to demonstrate the margin associated with the Kewaunee containment design, an upper bound calculation has been performed for additional steam generator heat release following re-flood. For the purpose of the calculation, it is assumed that a two-phase mixture is present in the steam generator tubes following re-flood. This mixture is assumed to remain in the tubes and to boil until all the available secondary side energy has been removed to the containment.

At end of re-flood, 56.2E+6 Btu available secondary side energy remains in the two steam generators. Of this total, 21.6E+6 Btu is contained in the broken loop steam generator and 34.6E+6 Btu in the intact loop steam generator. Froth boiling in the steam generator tubes is the mechanism for removal of this energy. These boiling rates plus the decay heat addition constitute the potential energy release to the containment.

The rate of froth boil-off in each of the steam generators is calculated by balancing the loop resistance to steam flow against the available driving ΔP. In the intact loop, steam flow results from the density difference between the downcomer and the core. The broken loop steam flow results from a density difference between downcomer and core plus a pressure difference between downcomer and containment due to the pressure drop across the broken loop reactor coolant pump.

The effect of decay heat addition as core steam generation is accounted for in the driving ΔP calculation and the steam flow calculation for each loop.

The effects of safety injection flow and auxiliary feedwater flow on the containment energy release rate are also considered in the calculation. The addition of injection water to the reactor coolant system acts to reduce the energy release rate by the heat required to raise this water to saturation temperature at containment pressure. This reduction is included in the release rate calculation and is supported by the results of steam/ECCS water mixing tests. Auxiliary feedwater flow to the secondary side of each steam generator does not affect the energy release rate but does reduce the available steam generator energy by the heat required to raise the feedwater to saturation temperature at containment pressure.

The post re-flood containment mass and energy release rate transient generated by the froth calculation is presented in Table 14C-8. The calculation is for the 3-ft^2 pump suction break and is started at end of 10-foot entrainment time. The decrease in release rates at 500 seconds results from the cooldown of the broken loop steam generator. Intact loop steam generator cooldown occurs at 1040 seconds. At this time all secondary side steam energy has been removed and the froth calculation is terminated.

A recalculation of the containment pressure transient using the data given in Table 14C-8 results in a peak pressure of 43.6 psig.

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14C.2 Sensitivity Studies

Containment Initial Conditions and Active Heat Removal Initiation Times

In addition to the analyses discussed in sections 14.3 and 14C.1, an analysis has been performed using a more conservative set of containment initial conditions and analysis input parameters. This analysis was performed using the well-known CONTEMPT-LT/26 computer code. The design basis 3.0 ft^2 reactor coolant pump suction break was the analyzed event. Three combinations of active heat removal systems were considered including one internal containment spray train with two containment fan coil units, two internal containment spray trains, and four containment fan coil units.

The containment initial conditions used in the analysis included the following:

a. Since the KNPP Technical Specifications allow a normal operation containment pressure accumulation of up to 2 psig, an initial containment pressure of 16.85 psia was used in the calculation. The 16.85 psia allows for the 2-psig accumulation plus 0.15 psi for instrument tolerance.

b. The active heat removal equipment cooling initiation times were revised from the previously assumed 60 seconds to reflect additional conservatism. The values used for these inputs were 135 seconds for internal containment spray cooling initiation and 85 seconds for containment fan coil unit cooling initiation.

c. The containment fan coil unit heat removal rates were revised to account for cooling water temperatures, which may exceed the 66°F originally used in the analysis. A cooling water temperature of 85°F was selected.

d. The passive structural heat sinks have been revised to reflect the as-built configuration of concrete structures. Table 14C-7 and Table 14C-9 were used as the passive heat sinks. Additionally, the effect of paint and other coatings was accounted for in modeling the passive heat sinks.

Using the blowdown and re-flood mass and energy additions shown in Figures 14C-5 and 14C-6, and the post re-flood mass and energy releases shown in Table 14C-8, the predicted maximum peak pressure is 45.2 psig for the case of two internal containment spray trains functioning. With one internal containment spray system and two containment fan coil units, the peak containment pressure is 45.0 psig, and for the case of four fan coil units, the peak pressure was 44.7 psig. All the calculated peak containment pressures are below the design value of 46 psig. It is noted that the assumption of 2.15-psig initial containment pressure is very conservative and, therefore, there is sufficient margin in existence for the containment peak pressure post-LOCA. The analysis results are plotted on Figure 14C-19.

14C.3 REFERENCES - APPENDICES 14A, 14B & 14C

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7. WM Kays, "Numerical Solutions for Laminar-Flow Heat Transfer in Circular Tubes" Trans. ASME, Vol. 77, 1955, pp. 1265-2374

8. HC Hottel, "Radiation Heat Transmission" Ch. 4 of Heat Transmission by W. H. McAdams, McGraw-Hill, 1954

9. LA Bromley, "Heat Transfer in Stable Film Boiling" Chemical Engineering Progress, Vol. 46, No. 5, 1950, pp. 221-227

10. YY Hsu, SW Westwater, "Approximation Theory for Film Boiling on Vertical Surfaces" Chemical Engineering Proress Symposium, Storrs, Conn., 1959

11. FW Dittus and LMK Boelter, University of California (Berkeley), Pbls. Eng., 2. 443 (1930)

12. RV Macbeth, "Burnout Analysis, Pt. 2, The Basis Burn-out Curve" U.K. Report AEEW-R 167, Winfrith (1963). Also Pt. 3, "The Low-Velocity Burnout Regimes" AEEW-R 222 (1963); Pt. 4, "Application of Local Conditions Hypothesis to World Data for Uniformly Heated Round Tubes and Rectangular Channels" AEEW-R 267 (1963)

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14. Crane Company, "Flow of Fluids Through Valves, Fittings, and Pipe" Technical Paper, No. 410

TABLE 14C-1 SUMMARY OF HEAT TRANSFER CORRELATIONS USED TO CALCULATE STEAM GENERATOR HEAT FLOW IN THE SATAN CODE	
Primary to Secondary Heat Flow	
Primary (Tube Side)	Secondary (Shell Side)
Dittus-Boelter	Jens Lottes
Secondary to Primary Heat Flow	
Primary	Secondary
Jens-Lottes Dougall-Robsenow McEligot	McAdams

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TABLE 14C-2 SENSITIVITY OF CORE STORED ENERGY TO POWER LEVEL, # NODES IN THE PELLET AND FUEL DENSIFICATION		
Power (kw/ft)	**Number of Radial Nodes in Pellet**	**Stored Energy (# full power seconds)**
I. Instantaneous Isotropic Densification to 96.5% TD		
7.66 *	10	6.59
7.66	1 (average pellet)	6.53
15.04	10	6.80
15.04	1	6.69
II. No Densification (92% TD)		
7.66	1	5.78

* This value of kw/ft can be considered a typical core average power and was used as a reference value for core stored energy calculations.

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TABLE 14C-3 AVAILABLE CORE ENERGY DURING BLOWDOWN		
(3.0 ft² Pump Suction)		
1.	Power = 1755.8 * Megawatts Thermal	
2.	Initial Core Stored Energy Above Average Coolant Temperature – Equivalent to 7.94 Full Power Seconds	13.32E+6 Btu
3.	Initial Core Stored Energy From Average Coolant Temperature to Saturation Temperature of Containment	3.35E+6 Btu
4.	Core Power Generation (To End of Blowdown)	4.54E+6 Btu
5.	Total Core Energy Available During Blowdown (3.0 ft²)	21.21E+6 Btu

* 102% of Engineered Safeguards Design Rating

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TABLE 14C-4 RESISTANCES USED IN REFLOOD ANALYSIS	
Component	**Resistance**
Core	17.0
Hot Leg	0.83/1.3
Steam Generator and Pump Suction Piping	4.10
Pump	3.83
Cold Leg Piping	0.4
Annulus	0.65

Resistances shown are the values of K used in the equation $\Delta H = K (V^2/2g)$. All velocities are based on a flow area of 4.5 ft^2 except in the core where an area of 26.9 ft^2 is used.

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TABLE 14C-5 SUMMARY OF RESULTS				
Break	**Length of Blowdown (sec)**	**Blowdown Energy Release (E+6 Btu)**	**Blowdown Peak Pressure (psig)**	**Peak Containment Pressure (psig)**
Double-Ended Pump Suction (10.5 ft^2)	12.8	169.73	38.5	42.8
0.6 Double-Ended Pump Suction(6.3ft^2)	14.7	169.06	38.2	42.7
4.0 Square Foot Pump Suction	18.0	166.29	37.95	42.66
3.0 * Square Foot Pump Suction	22.2	167.29	38.2	43.05
2.0 Square Foot Pump Suction	27.5	160.51	37.45	42.49
Double-Ended Cold Leg (8.25 ft^2)	12.1	170.52	39.4	40.9
Double-Ended Hot Leg (9.2 ft^2)	12.4	166.38	39.7	39.7
0.6 Double-Ended Hot Leg (5.5 ft^2)	14.8	164.87	39.32	39.34

* Worst Break

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TABLE 14C-6 REFLOOD ENERGY RELEASE (E+6 BTU)				
Break	**2.0 Ft² Pump Suction**	**Double-Ended Cold Leg**	**Double-Ended Hot Leg**	**0.6 Double-Ended Hot Leg**
Safeguards Assumption	Maximum	Maximum	Maximum	Maximum
Height in Core Entrainment Is Assumed to Stop (Feet)	8.0	8.0	8.0	8.0
Energy Types:				
1. Core Stored	3.56	8.14	7.11	6.3
2. Decay Heat	10.53	13.52	10.09	9.84
3. Thin Metal (Internals)	5.19	5.19	5.19	5.19
4. Thick Metal (Reactor Vessel Metal)	3.18	3.18	3.18	3.18
5. Steam Generator Energy	24.20	12.36	4.89	6.13
Total Reflood Energy to Containment at End of Entrainment	48.46	44.43	33.88	33.09

TABLE 14C-6
REFLOOD ENERGY RELEASE (E+6 BTU)

Break	Double-Ended Pump Suction	0.6 Double-Ended Pump Suction	4.0 Ft^2 Pump Suction	3.0 Ft^2 Pump Suction
Safeguards Assumption	Maximum	Maximum	Maximum	Maximum
Weight in Core Entrainment Is Assumed to Stop (Feet)	8.0	8.0	8.0	8.0
Energy Types:				
1. Core Stored	8.71	8.22	7.93	5.51
2. Decay Heat	11.78	11.56	11.22	10.87
3. Thin Metal (Internals)	5.19	5.19	5.19	5.19
4. Thick Metal (Reactor Vessel Metal)	3.18	3.18	3.18	3.18
5. Steam Generator Energy	18.22	18.98	19.44	22.30
Total Reflood Energy to Containment at End of Entrainment	49.04	49.13	48.60	49.04

TABLE 14C-7
STRUCTURAL HEAT SINKS

Linings	Material	Exposed Area (Ft^2)	Thickness (In.)
Containment Cylinder	Carbon Steel	41,300	1.5
Containment Dome	Carbon Steel	17,300	0.75
Reactor Vessel Liner	Carbon Steel Concrete Backup	1,260 1,260	0.25 12.00
Refueling Canal	Stainless Steel Concrete Backup Stainless Steel Concrete Backup	1,100 1,100 5,500 5,500	0.1875 12.0 0.25 12.0
Steel Structures The following items consist of various thicknesses and exposed areas. The cumulative exposed area contribution of all of the identified items are grouped by thickness value.			
Steam Generator Supports Pressurizer Support Reactor Coolant Pump Supports Crane Crane Rail Seismic Restraints Hangers	Carbon Steel	4,055 16,925 28,500 2,000 500	0.336 0.5 0.75 1.5 2.0
Handrails	Carbon Steel	1,695	0.145
Grating	Carbon Steel	12,400	0.09
Exposed Pipe	(None assumed for calculations)		
Exposed Conduit and Cable Trays	Carbon Steel	2,000	0.1
Ductwork	Carbon Steel	18,000	0.07
Accumulators	Carbon Steel	2,200	1.44
Note: Concrete structures inside containment not used in the calculation include:			
Heavy Walls	40,800 ft^2	12 in. thick	
Heavy Floors	25,070 ft^2	6 in. thick	
Light Floors	7,570 ft^2	3 in. thick	

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TABLE 14C-8 POST REFLOOD MASS AND ENERGY RELEASE		
Time	Mass Release Rate (lbm/sec)	Energy Release Rate (Btu/sec)
320.00	149.06	182537.27
340.00	149.92	83604.17
360.00	146.75	179697.39
380.00	145.80	178532.94
400.00	141.81	173694.77
420.00	145.86	178604.19
440.00	141.87	173768.34
460.00	141.00	172693.45
480.00	137.97	168965.83
500.00	137.06	167846.64
520.00	67.34	80708.04
540.00	75.30	90330.07
560.00	74.88	89813.55
580.00	73.02	87611.97
600.00	72.63	87139.44
620.00	72.30	86729.24
640.00	71.97	86335.15
660.00	71.66	85956.08
680.00	71.36	85591.01
700.00	71.07	85239.05
720.00	70.79	84899.37
740.00	70.52	84571.21
760.00	70.26	84253.89
780.00	70.01	83946.78

TABLE 14C-8 POST REFLOOD MASS AND ENERGY RELEASE		
Time	**Mass Release Rate (lbm/sec)**	**Energy Release Rate (Btu/sec)**
800.00	69.76	83649.29
820.00	69.55	83393.40
840.00	69.35	83144.98
860.00	69.15	82903.63
880.00	68.95	82669.03
900.00	68.77	82440.81
920.00	68.58	82218.67
940.00	68.41	82002.32
960.00	68.23	81791.49
980.00	68.06	81585.94
1000.00	67.90	81385.41
1020.00	67.62	81052.62
1040.00	67.36	80728.62

TABLE 14C-9 Concrete as Passive Heat Sink	
Concrete Floors	
Total Thickness, Inches	One Side Area, Ft^2 (one side exposure)
4	1785
6	2156
8	464
10	2405
12	6592
15	880
> 15	6512
Concrete Walls	
Total Thickness, Feet	One Side Area, Ft^2 (one side exposure)
1.0	238
1.167	846
1.667	175
1.75	144
2.0	1,842
> 2.0	21,900

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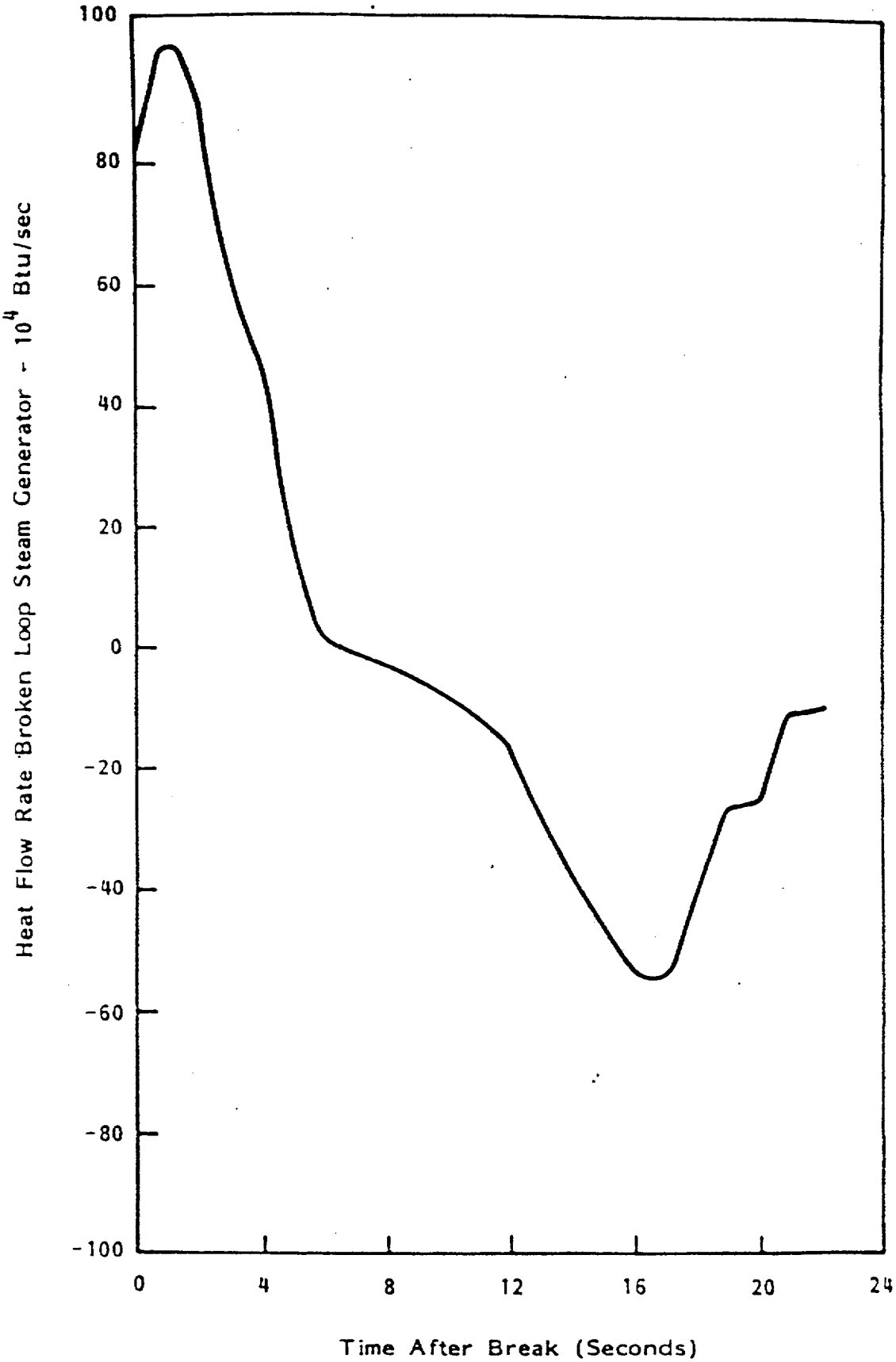

3.0 FT. SQ. PUMP SUCTION BREAK (SPLIT)
FIGURE 14C-1

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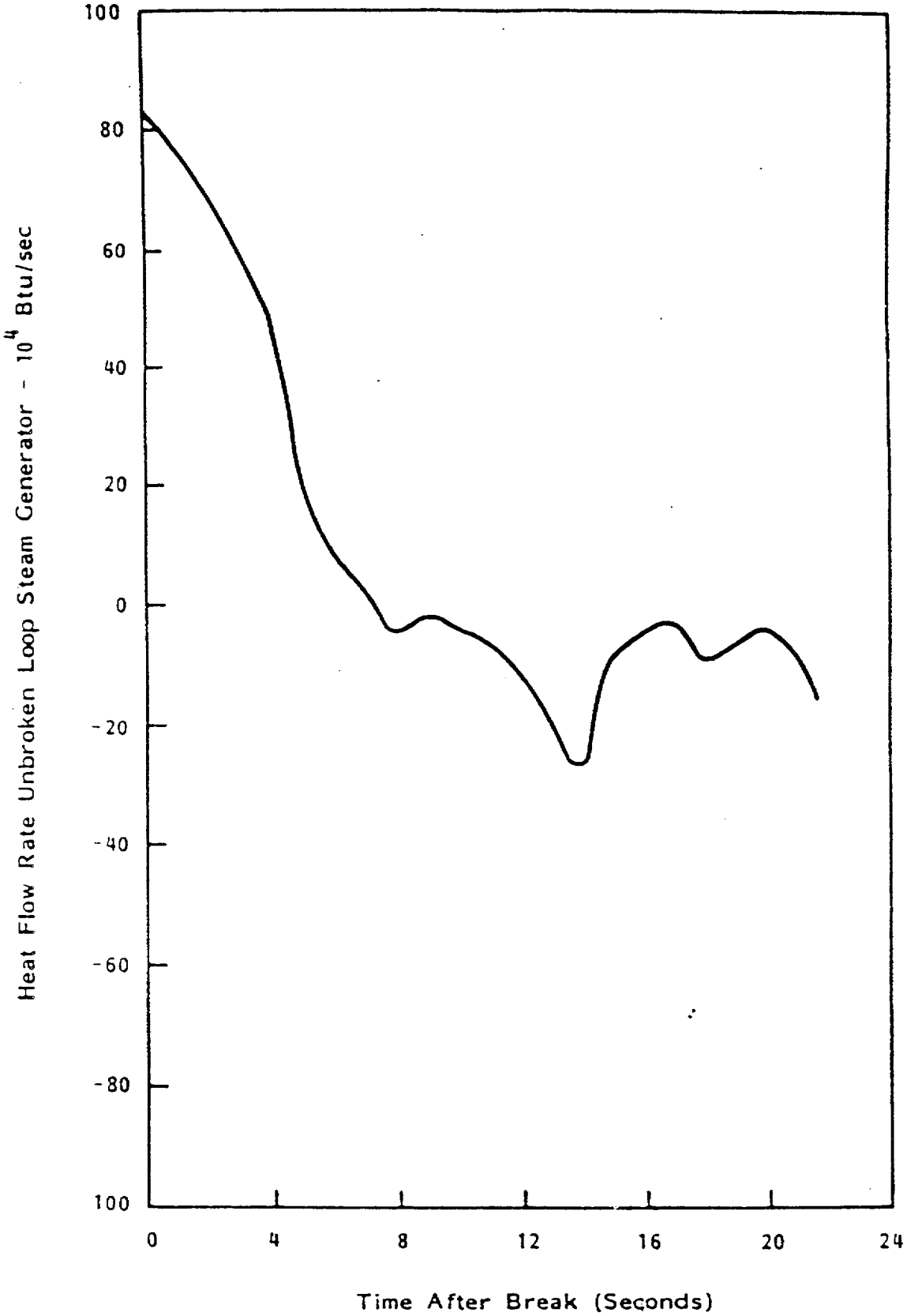

3.0 FT. SQ. PUMP SUCTION BREAK (SPLIT)
FIGURE 14C-2

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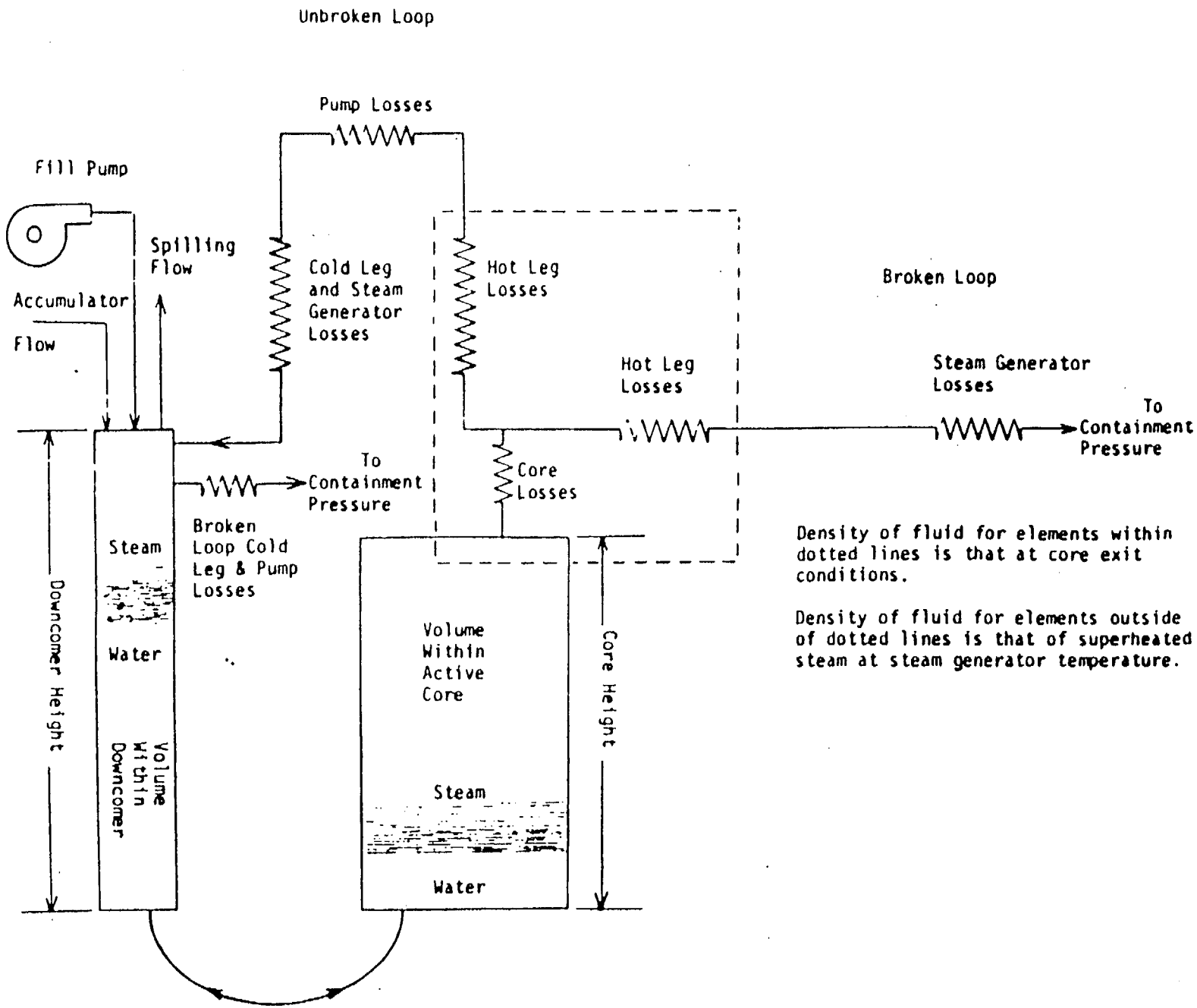

HYDRAULIC MODEL FOR PUMP SUCTION BREAK
FIGURE 14C-3

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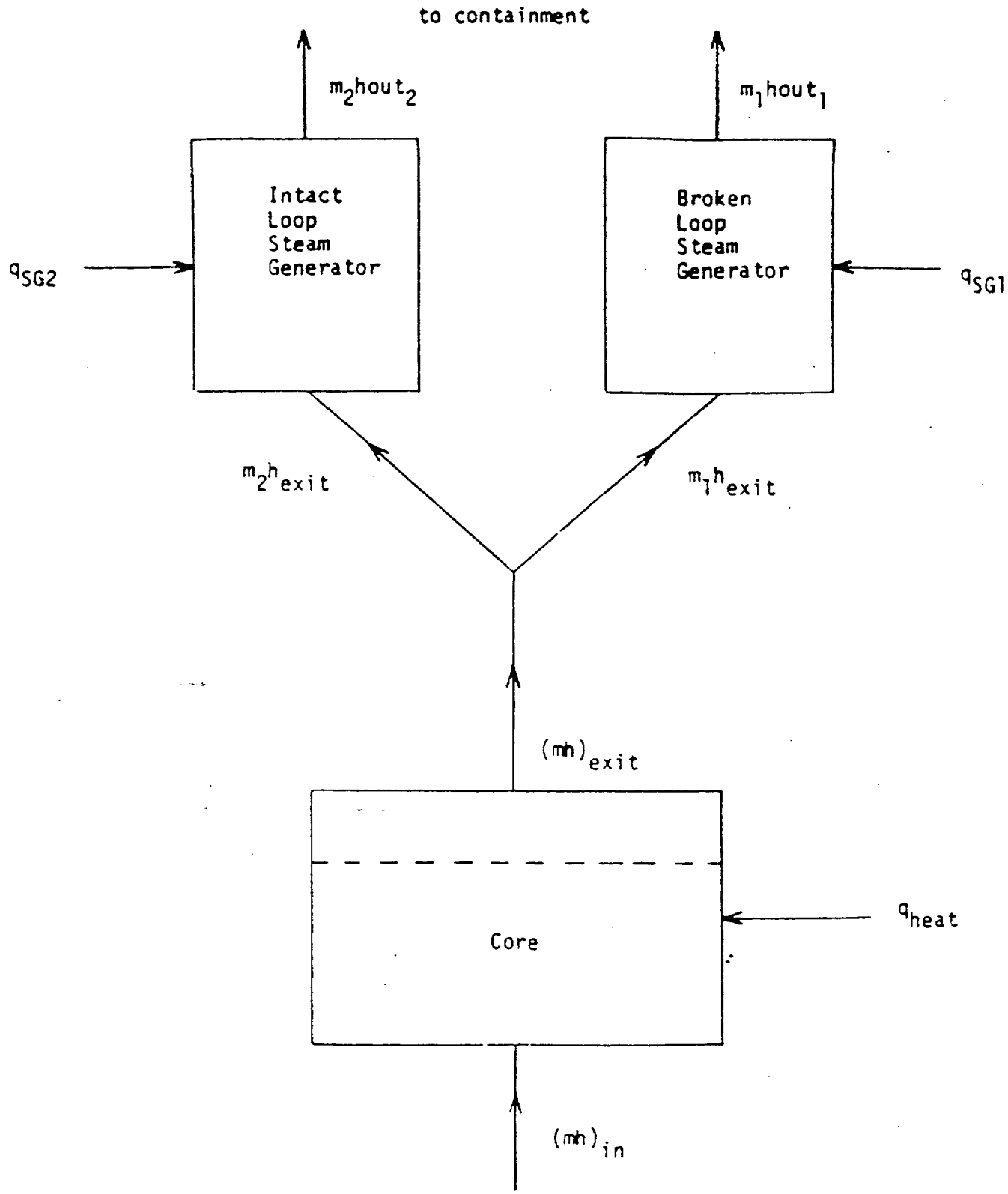

ENERGY BALANCE MODEL
FIGURE 14C-4

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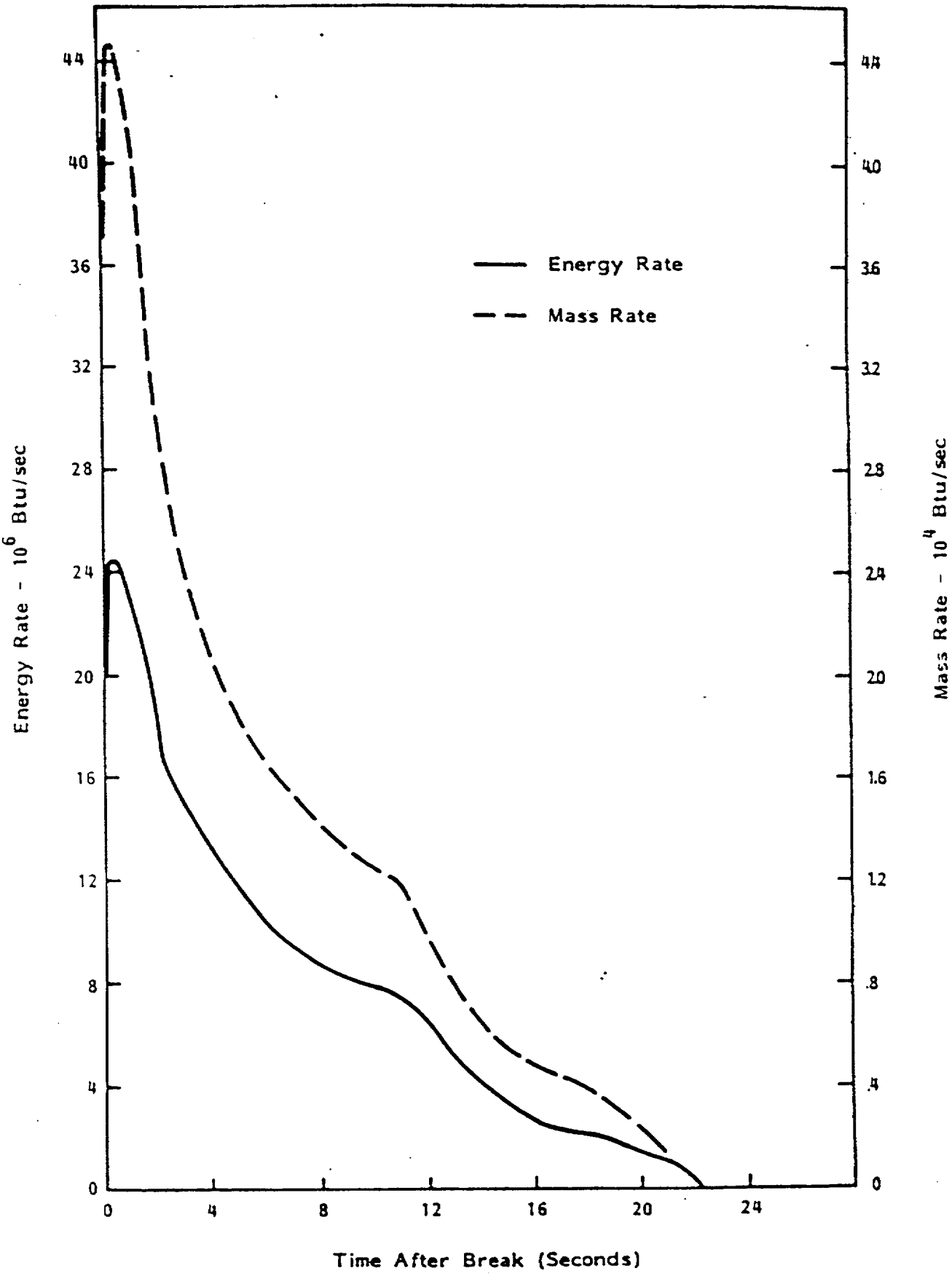

3.0 FT. SQ. PUMP SUCTION BREAK (SPLIT) MASS AND ENERGY RELEASED DURING BLOWDOWN

FIGURE 14C-5

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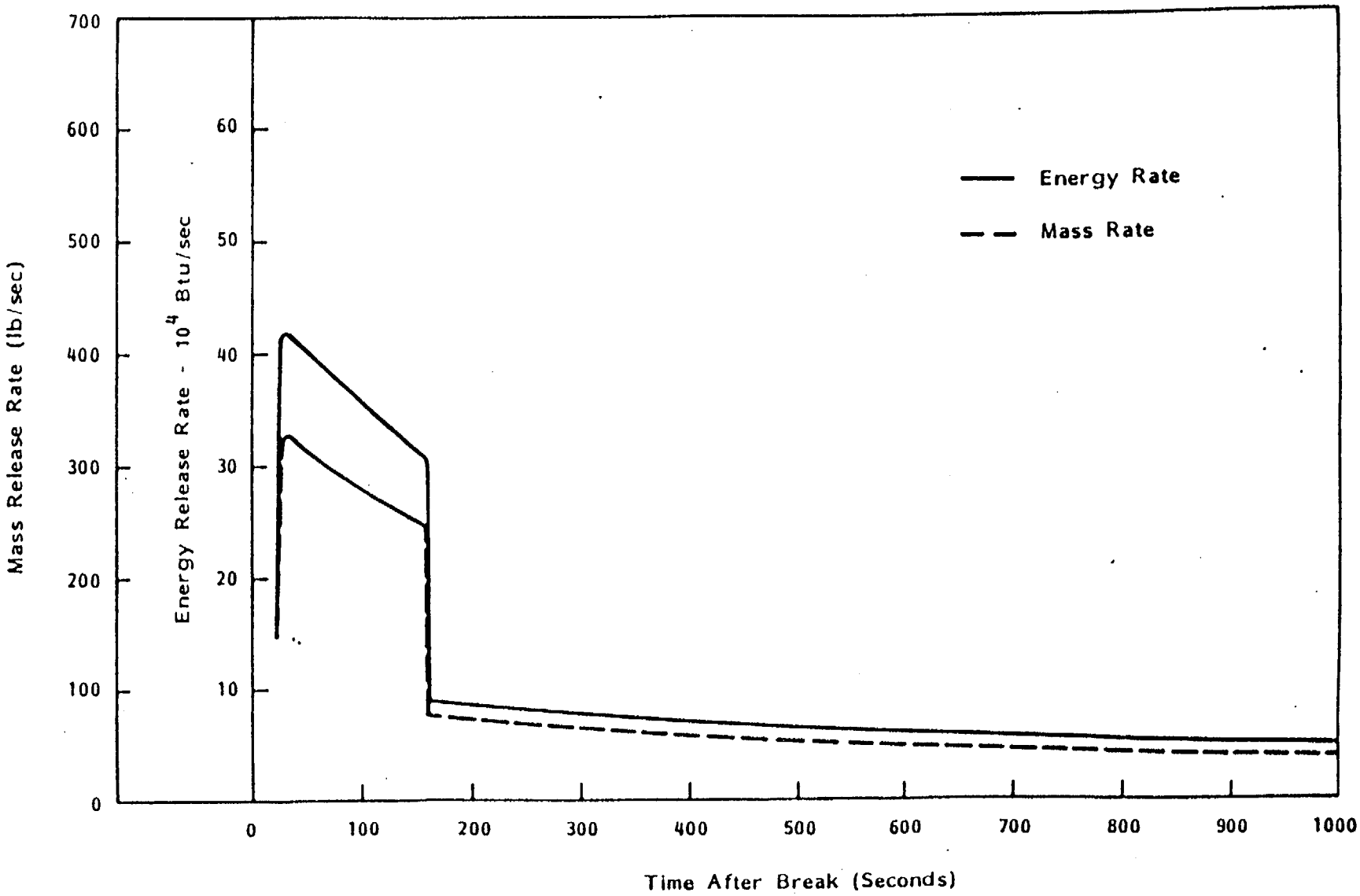

3.0 FT. SQ. PUMP SUCTION BREAK (SPLIT) MASS AND ENERGY RELEASED DURING REFLOOD

FIGURE 14C-6

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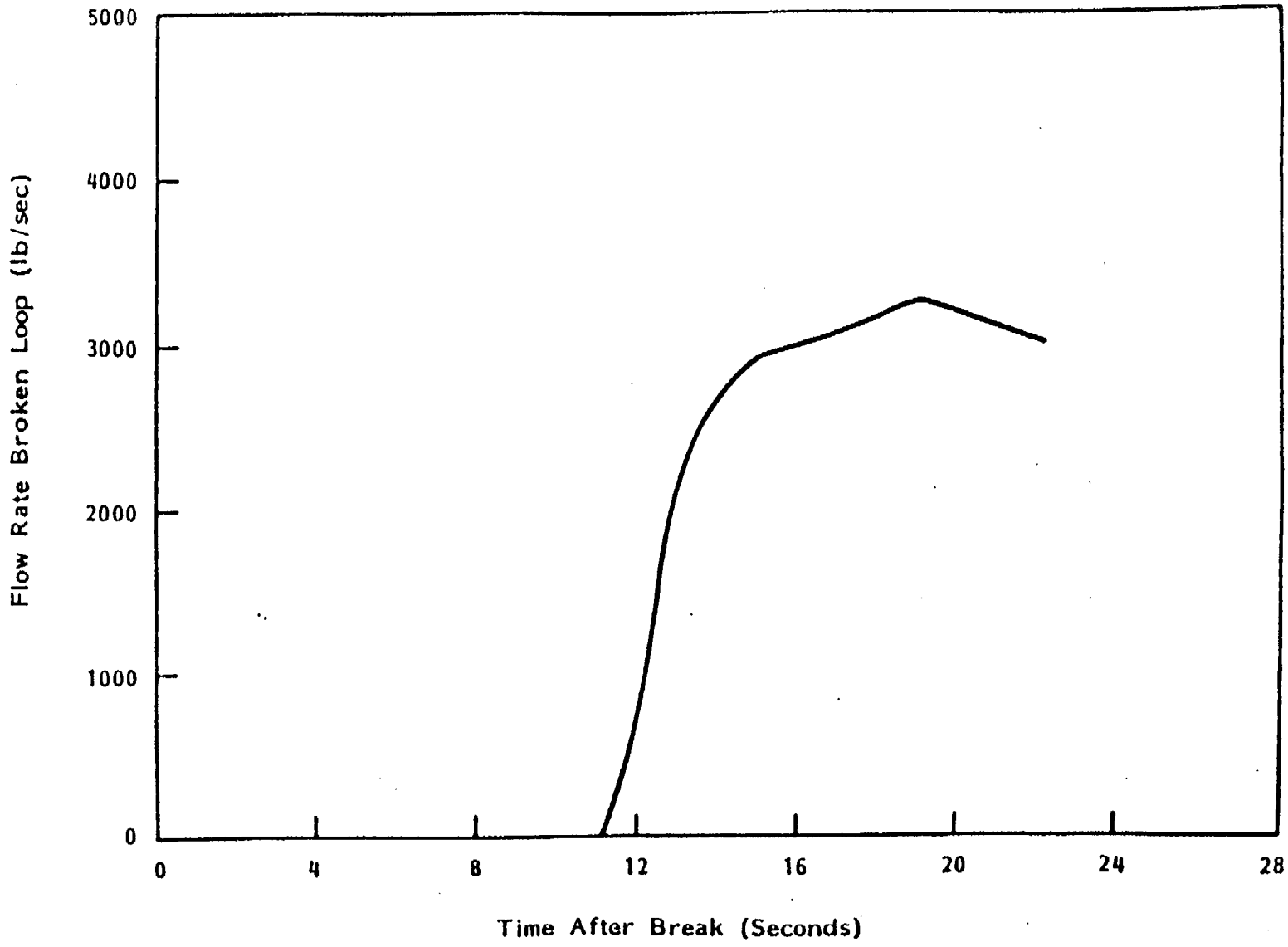

3.0 FT. SQ. PUMP SUCTION BREAK (SPLIT) ACCUMULATOR FLOW RATE
FIGURE 14C-7

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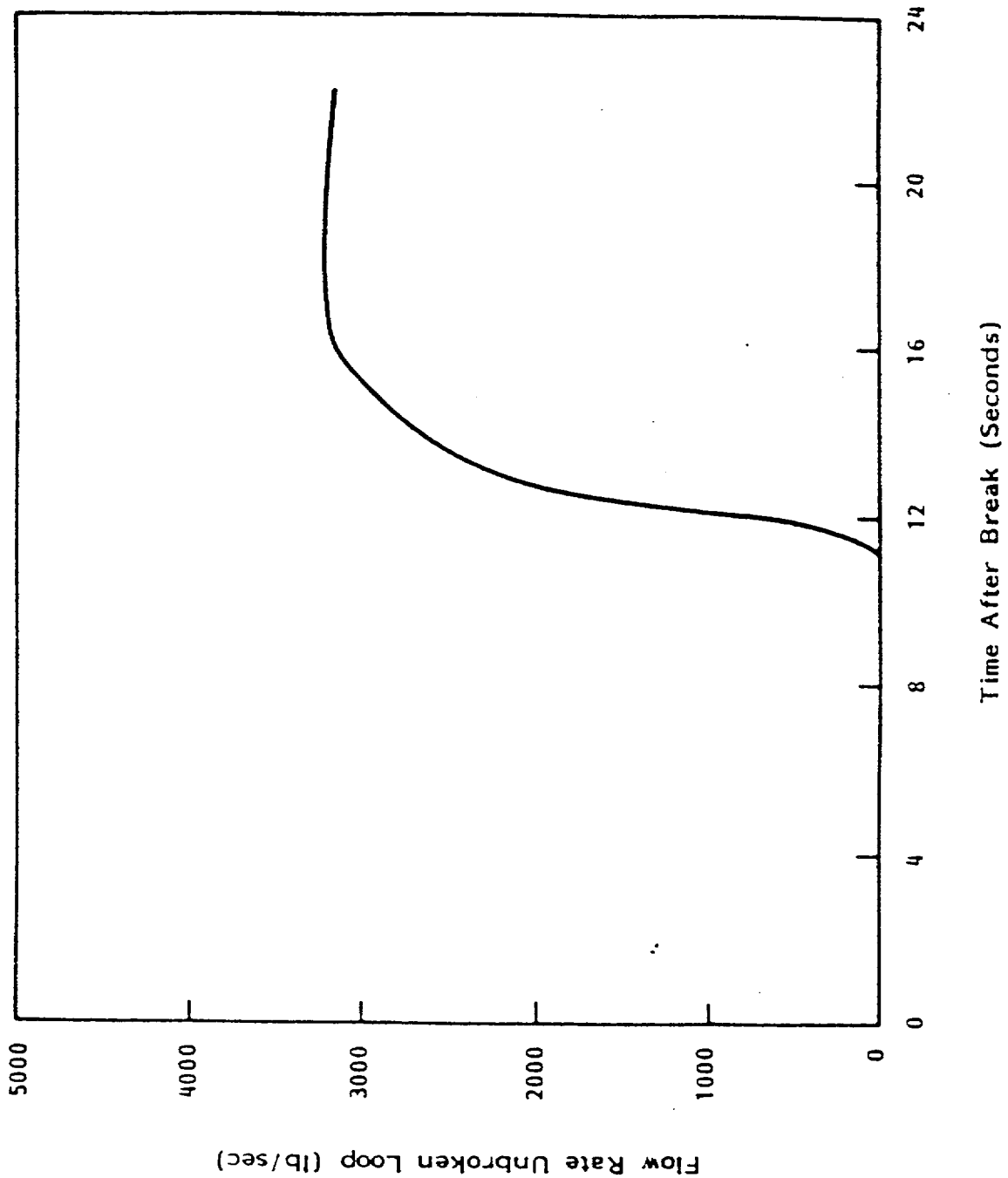

3.0 FT. SQ. PUMP SUCTION BREAK (SPLIT) ACCUMULATOR FLOW RATE
FIGURE 14C-8

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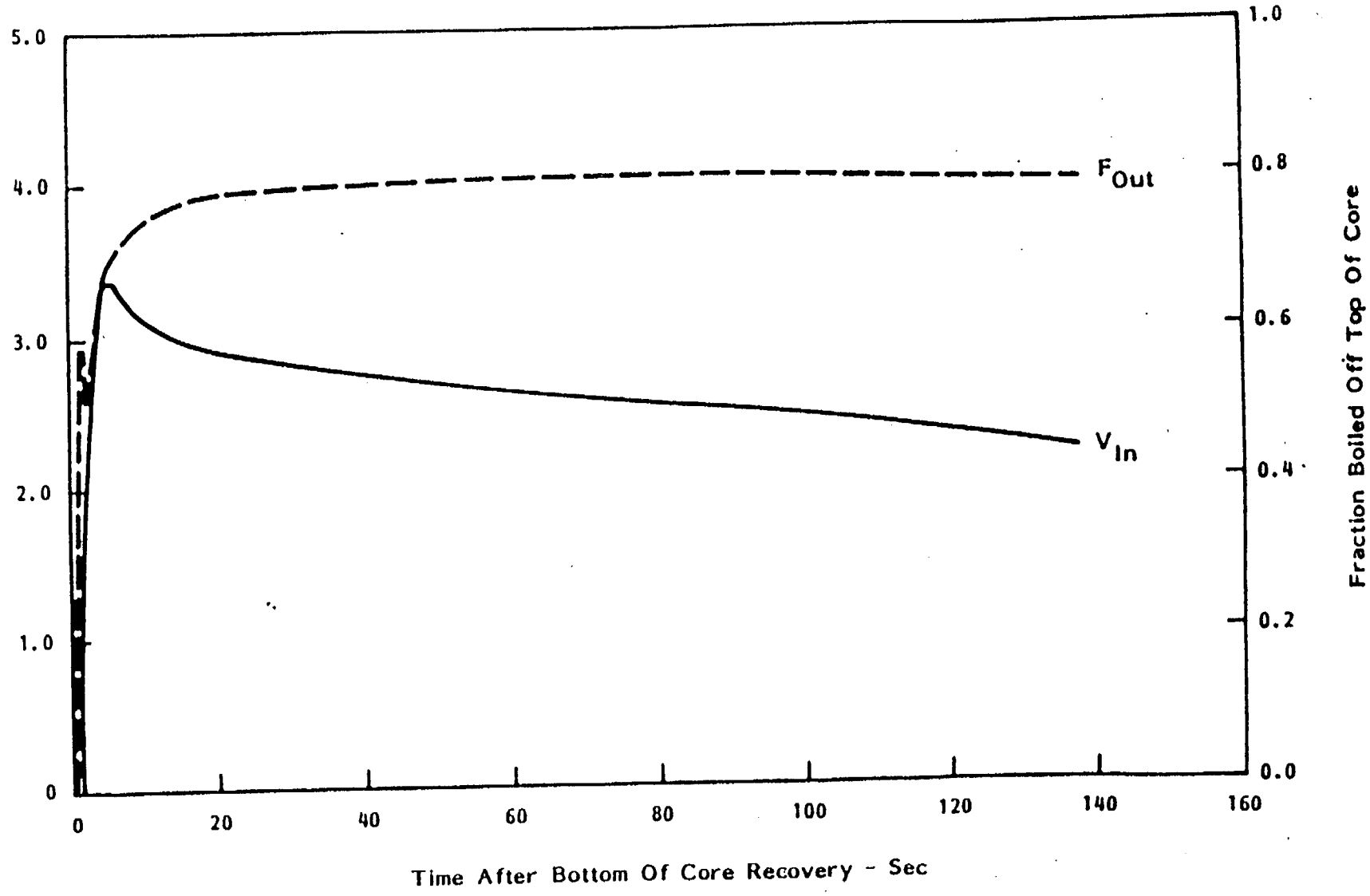

3.0 FT² PUMP SUCTION BREAK
FIGURE 14C-9

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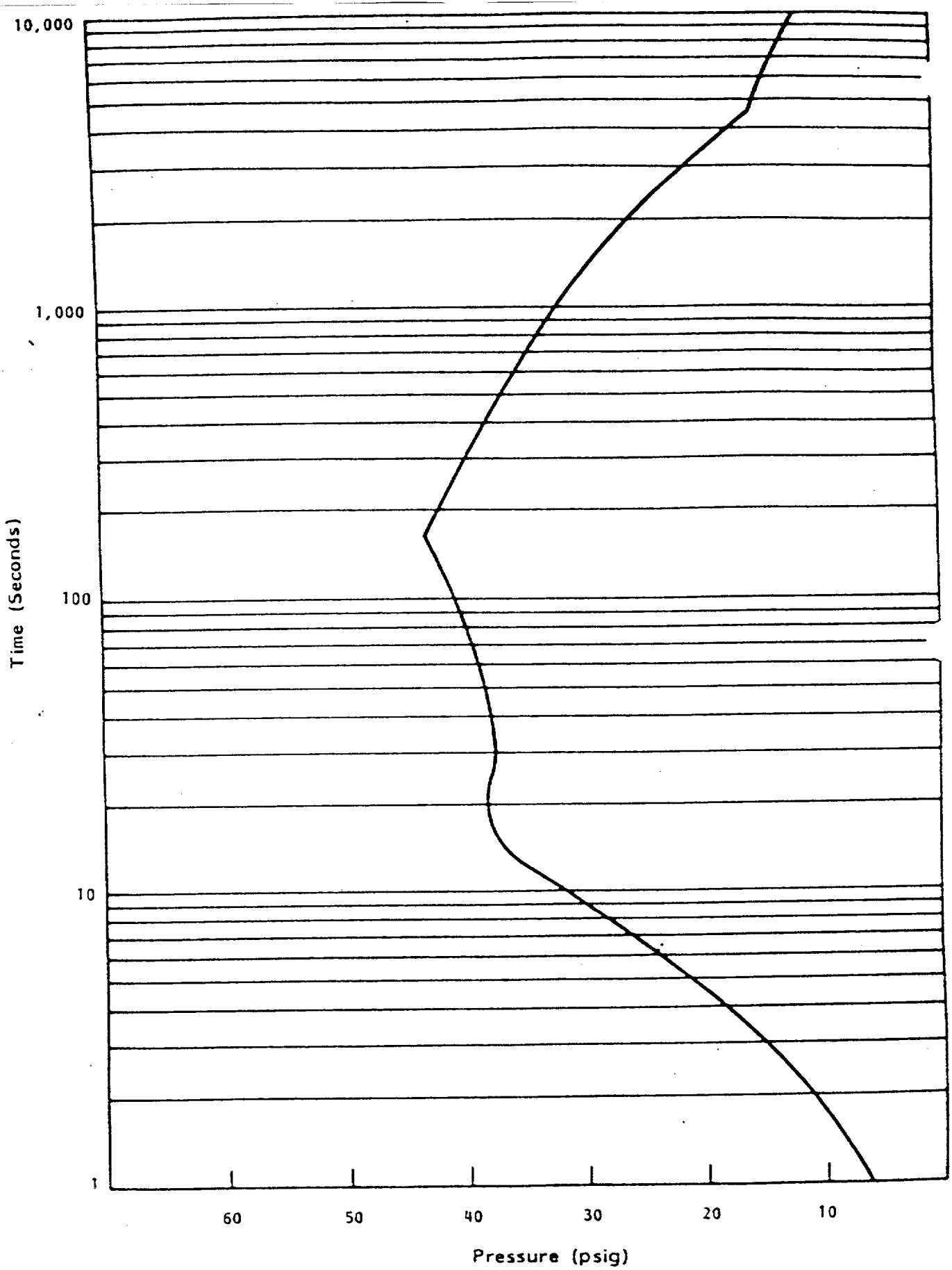

3.0 FT² PUMP SUCTION BREAK
FIGURE 14C-10

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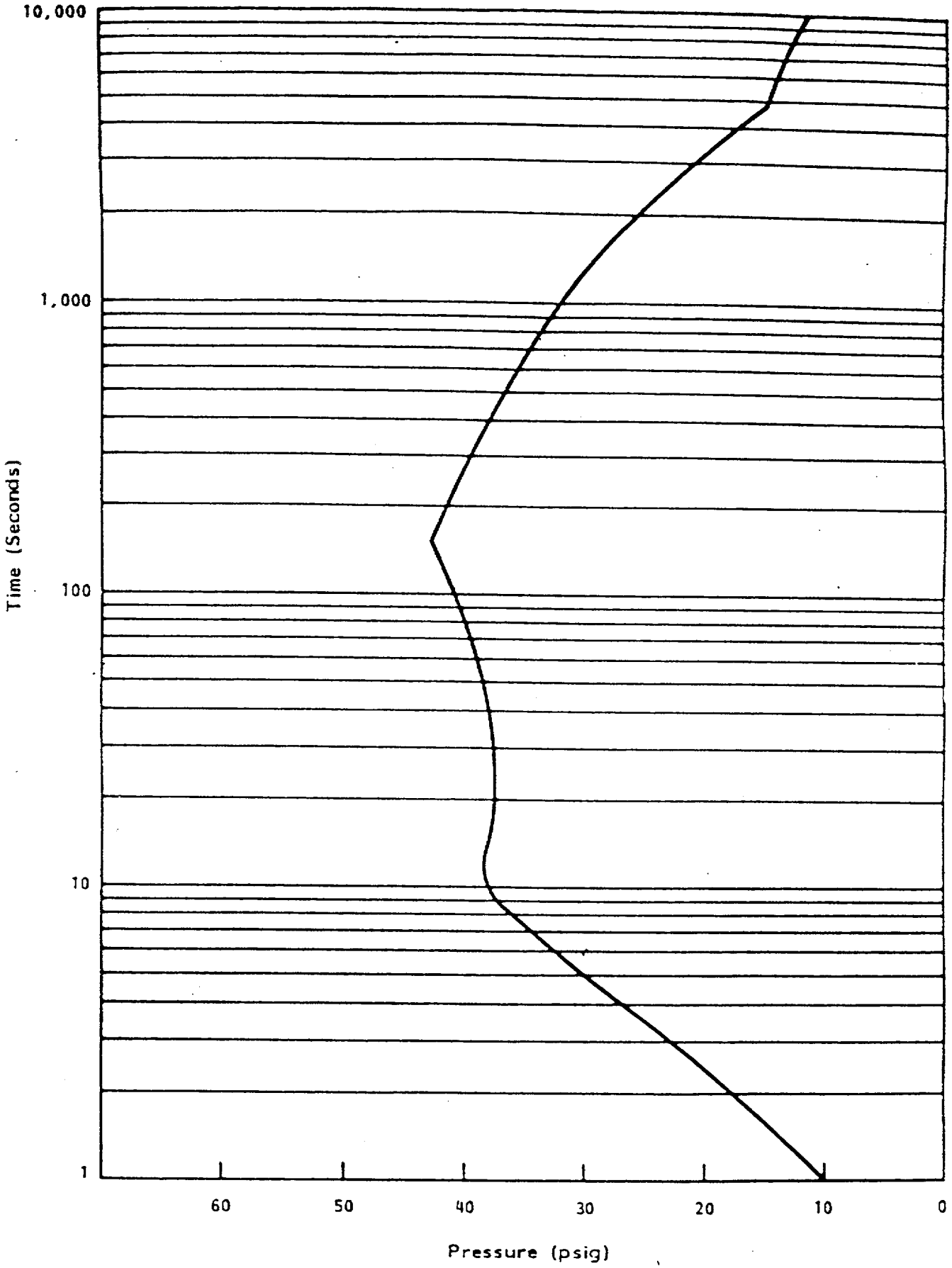

DOUBLE ENDED PUMP SUCTION BREAK
FIGURE 14C-11

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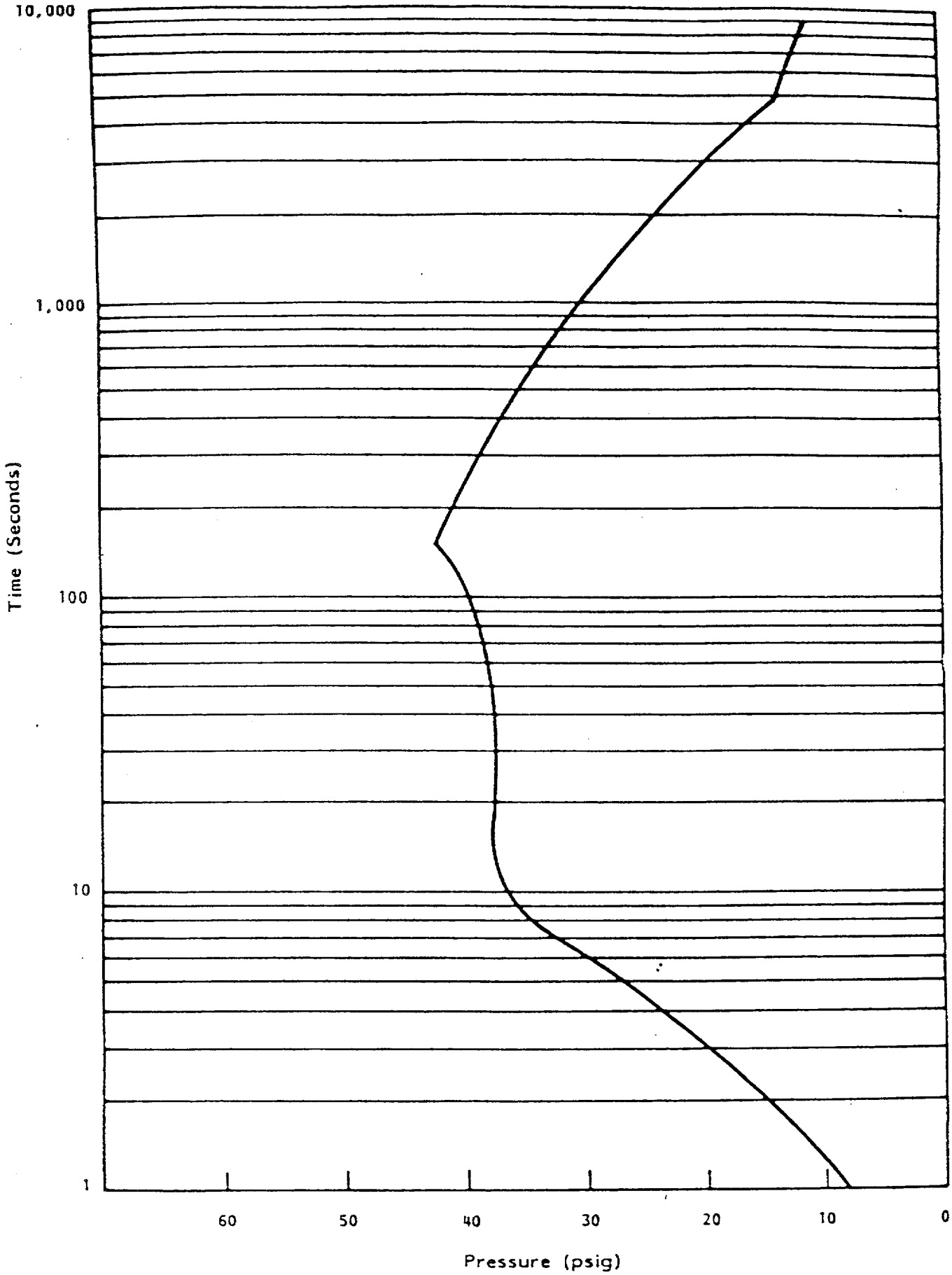

0.6 DOUBLE ENDED PUMP SUCTION
FIGURE 14C-12

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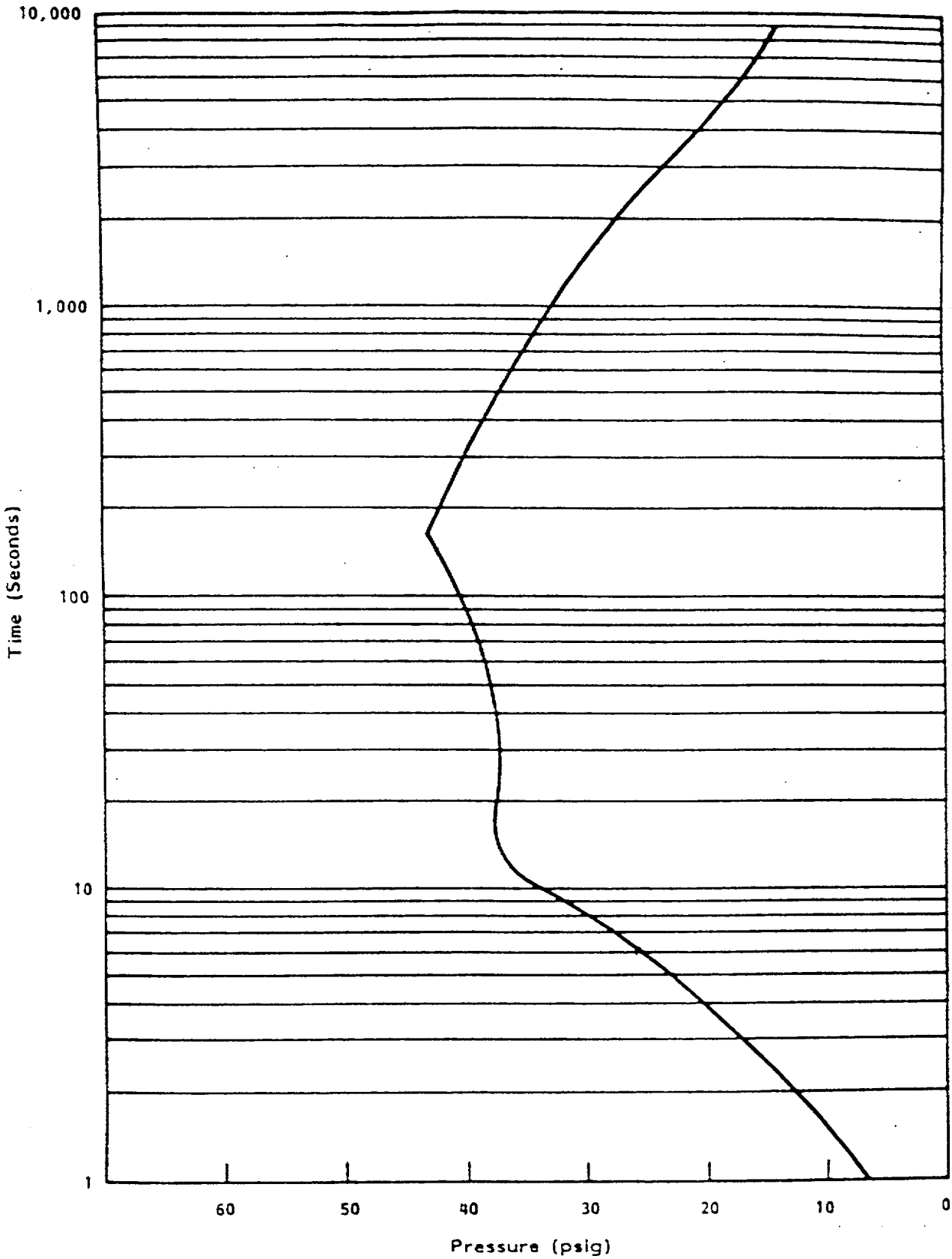

4.0 FT² PUMP SUCTION BREAK
FIGURE 14C-13

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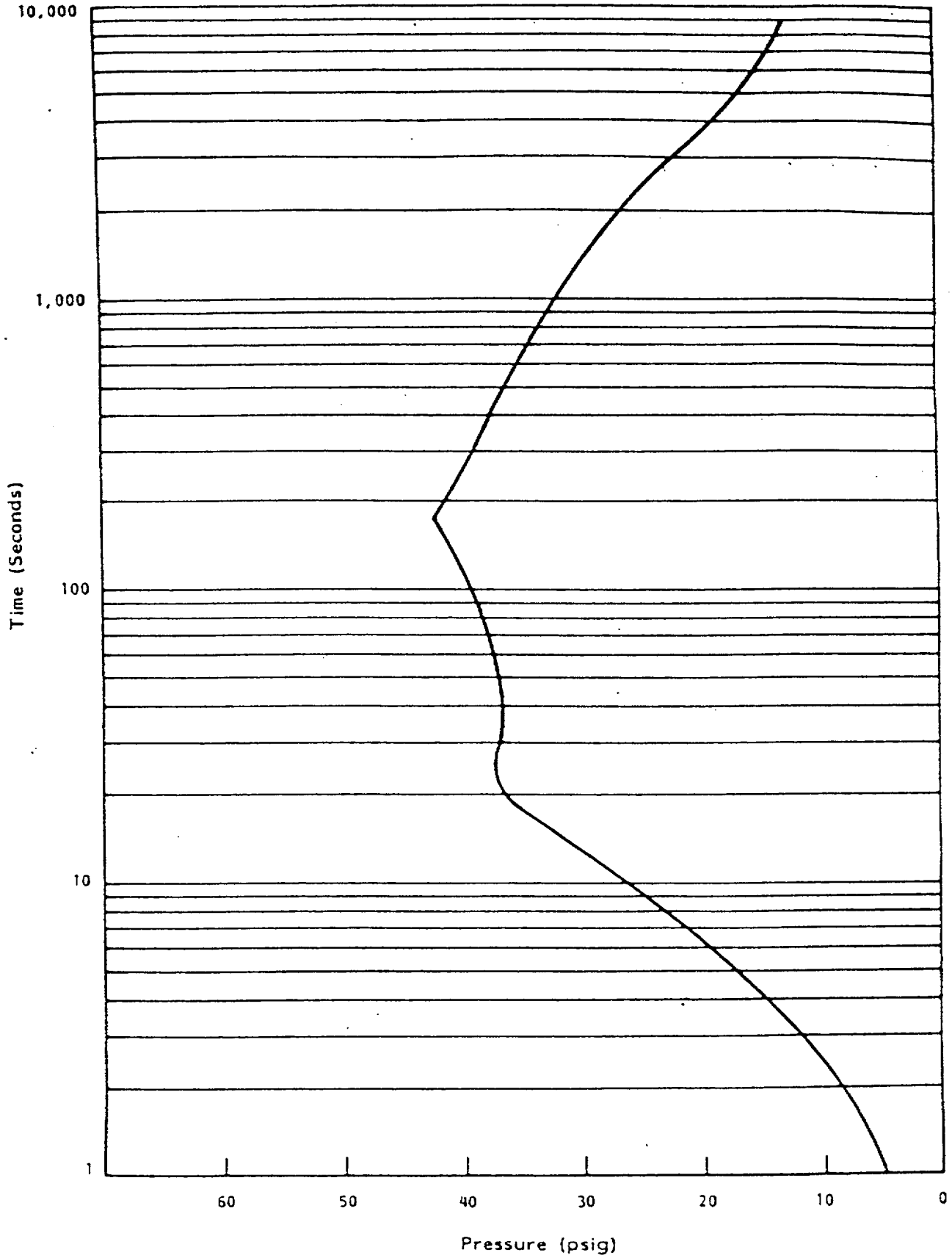

2.0 FT² PUMP SUCTION BREAK
FIGURE 14C-14

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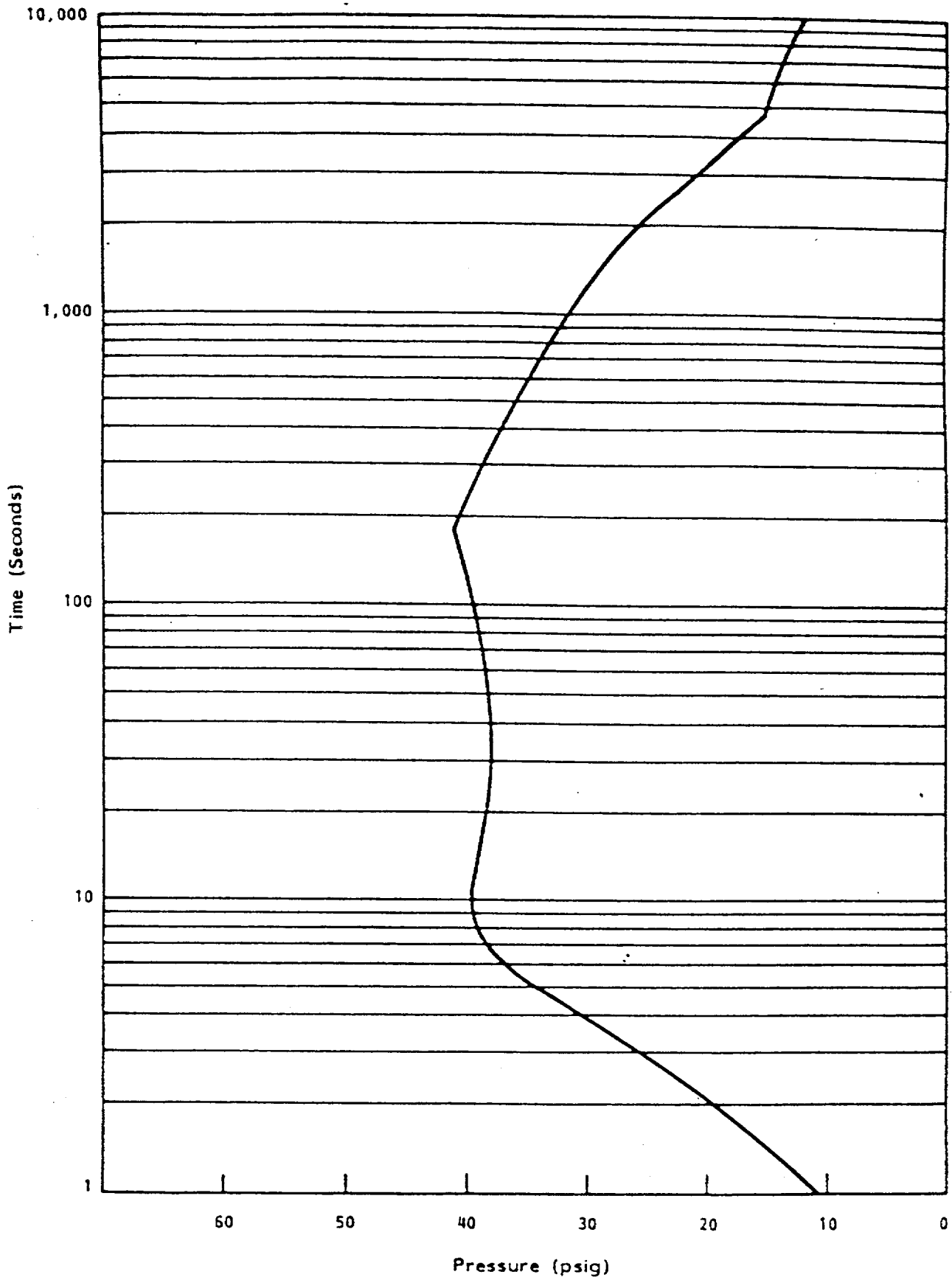

DOULBE-ENDED COLD LEG
FIGURE 14C-15

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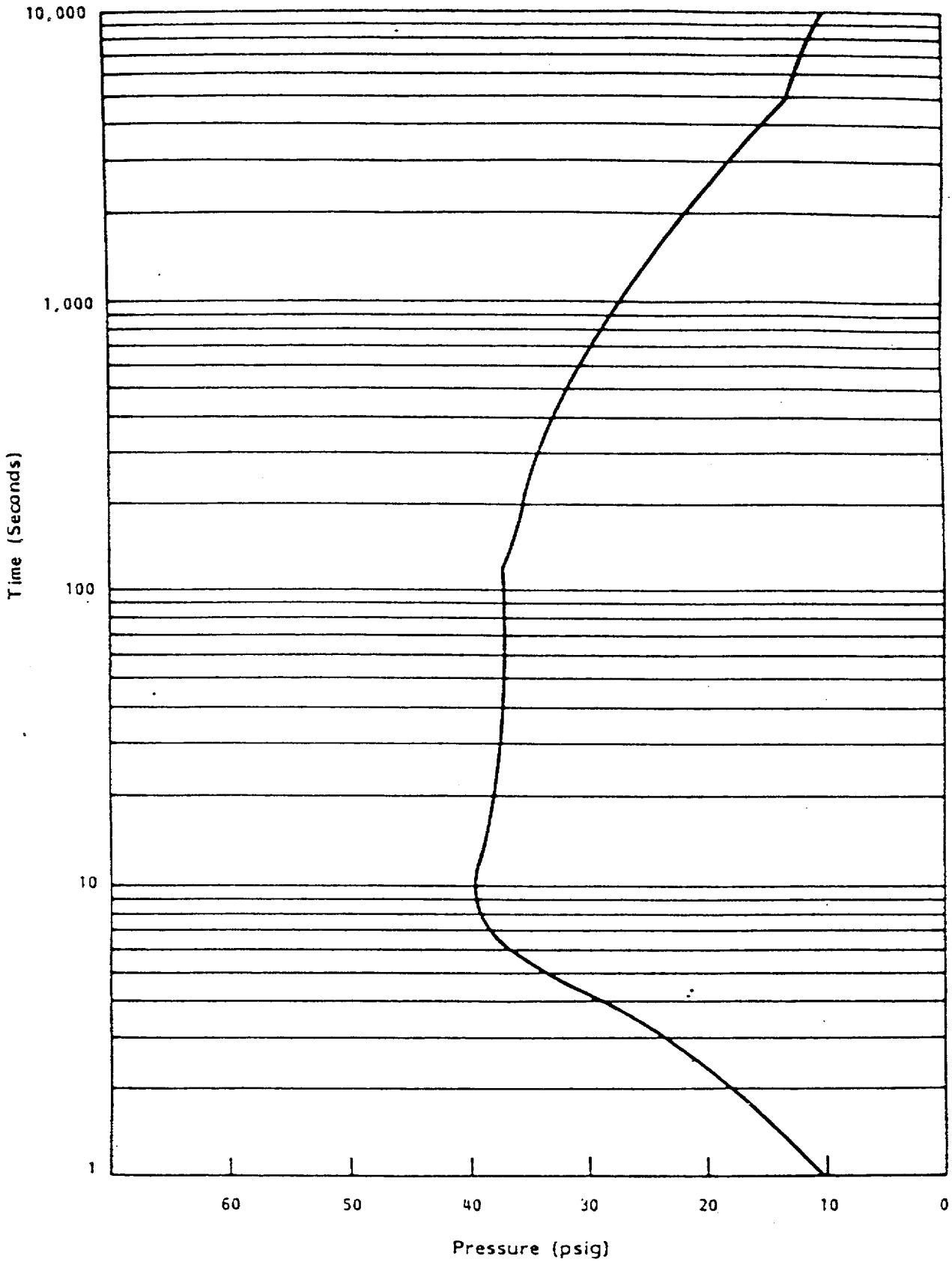

DOULBE-ENDED HOT LEG BREAK
FIGURE 14C-16

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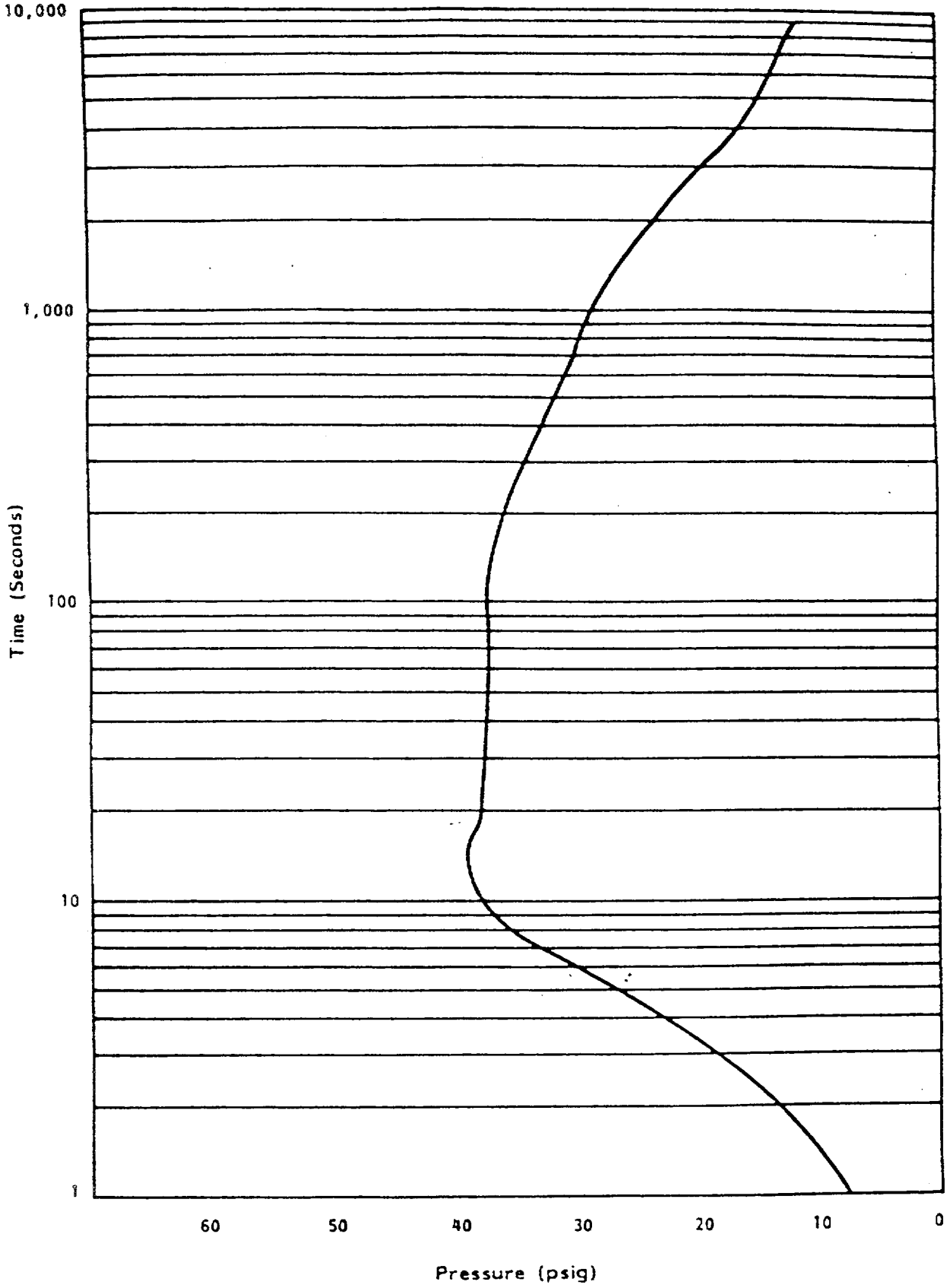

0.6 DOULBE-ENDED HOT LEG BREAK
FIGURE 14C-17

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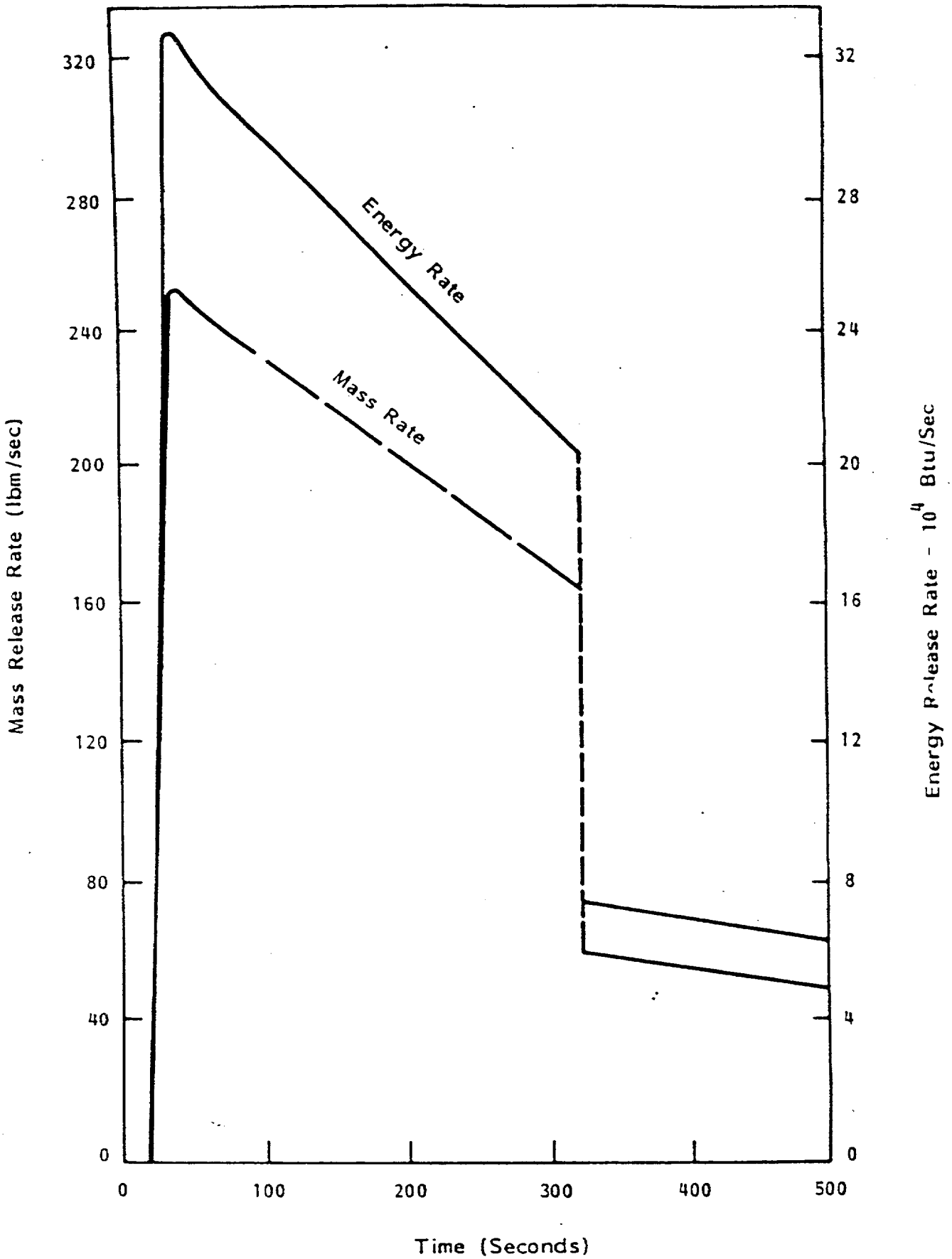

3.0 FT² BREAK (SPLIT) MASS AND ENERGY RELEASED
FIGURE 14C-18

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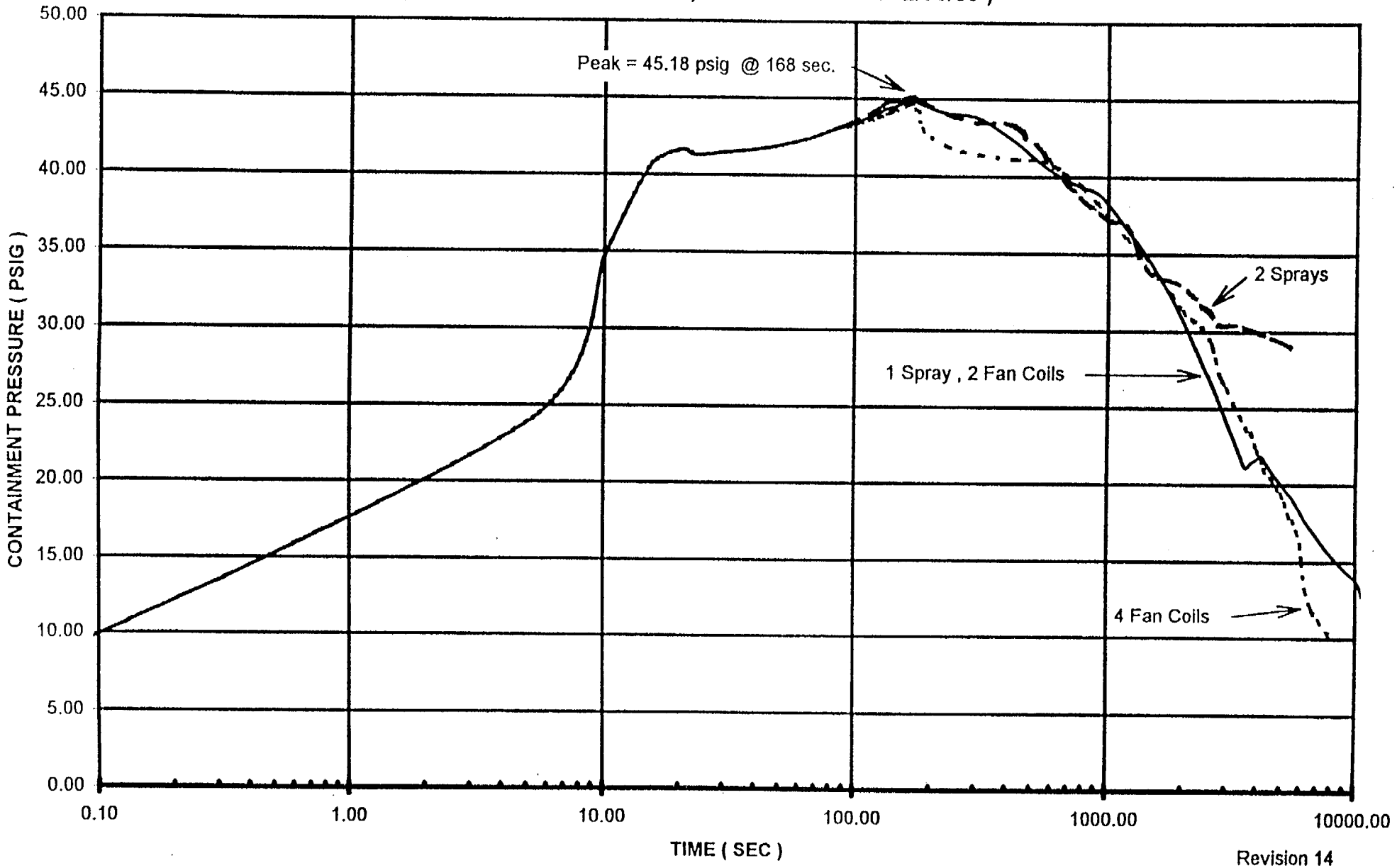
Figure 14C-19
Cntmnt Peak Pressures Sensitivity Study
(Calculation No. SSFI-23-1, Revision 0 dated 02/03/89)



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