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PROVISIONS FOR EMERGENCY CORE COOLING

DRESDEN UNIT 3

As Amend #5

August 10, 1966

PROVISIONS FOR EMERGENCY CORE COOLING

A. INTRODUCTION

In a previous draft memorandum dated July 26, 1966, entitled "Description and Evaluation of Dresden Unit 3 Emergency Core Cooling Provisions," it was stated that two core spray cooling systems and the high pressure coolant injection system are provided to assure continuity of core cooling for those postulated conditions wherein it is assumed that mechanical failures occur and coolant is partially or completely lost from the reactor vessel. These systems together with other design features of Dresden Unit 3 are provided to prevent fuel clad melting for the entire spectrum of assumed accidental conditions.

In addition to these provisions, it is now proposed that certain design changes be made to incorporate a low pressure coolant injection system (flooding system), independent of and in addition to the above emergency cooling provisions. As indicated in the information previously transmitted, it is the design objective of the emergency core cooling systems to prevent fuel clad melting and limit to a negligible amount any metal water reaction for all primary system mechanical failures up to and including those equivalent to the double ended severance of a recirculation pipe.

This memorandum summarizes the design features of the low pressure coolant injection system and identifies other design and operational considerations which assure high reliability for continuity of core cooling under the postulated accident conditions including provisions for adequate emergency electrical power supply, provisions for increased feedwater inventory, and further considerations of vessel internal integrity under postulated reactivity transients. A diagram of the several emergency core cooling systems is shown in Figure 1.

B. LOW PRESSURE COOLANT INJECTION SYSTEM

1. Design Basis

a. A low pressure coolant injection system is provided to prevent fuel clad melting as a result of various postulated but improbable loss of primary coolant accidents for a range of failure sizes from those for which the core is adequately cooled by the high pressure coolant injection system up to and including the design basis accident i.e., the instantaneous mechanical failure of a size equivalent to the largest primary system pipe.

b. The low pressure coolant injection system will be provided with redundancy of pumping capacity.

c. The low pressure coolant injection system shall operate without reliance upon external sources of power.

d. The low pressure coolant injection system shall be designed so that each component of the system can be tested and inspected periodically.

2. Description

The low pressure coolant injection system is designed to pump water directly from the suppression pool into the reactor vessel under accidental loss of coolant conditions.

This system also provides adequate containment spray cooling if ever required and will be specifically designed to provide required flow rate and pump discharge heads for low pressure coolant injection. A schematic diagram for the system is shown in Figure 2.

The system is provided with four pumps, any three of which will provide the design flow rate of 16,000 gpm from the pressure suppression pool through the heat exchangers to the space inside the vessel shroud. The system flow rate is established to accommodate possible leakage flows. Design modifications will be incorporated to minimize leakage caused by thermal differential expansions during flooding. The water will be pumped directly inside the shroud without employing a sparger or spray nozzles. At this time several arrangements have been evaluated and found to be satisfactory for coolant delivery within the shroud.

At this time, the preferred arrangement involves the injection of the flooding flow through eight penetrations in the bottom of the reactor vessel. Standpipes are incorporated within the vessel to insure maintenance of flooding capability within the shroud.

The design flow rate will assure injection of an adequate inventory of water inside the shroud to prevent fuel clad melting. Special design considerations will be given to the vessel-shroud configuration to assure component integrity under the postulated conditions of use.

The piping system will be fabricated of carbon steel from the suppression chamber to the outer containment isolation valve. Relief valves are utilized for pressure protection of this section of the system. From the outer isolation valve into the reactor the system is fabricated of stainless steel and designed for service at 1250 psig and 575°F.

All portions of the system will be designed in accordance with applicable codes.

This low pressure coolant injection system is designed to be placed into service automatically and concurrently with activation of one of the two core spray systems. The pumping equipment of this system will be activated on a signal of reactor "low low" water level similar to the pumping equipment of the core spray system. The valve opening action of this system will be activated on a preset low pressure signal again similar to the valving of the core spray system.

A test line capable of full system flow (16,000 gpm) is connected from the outside isolation valve back to the pressure suppression pool. Flow can be diverted into this line to test operability of the pumps and control system during reactor operation. As shown on Figure 2, water can also be pumped from the condensate storage tank to permit functional testing of the system periodically when the reactor is shutdown.

3. Summary of System Performance

a. Breaks Below Water Level (Liquid Flow)

Either of the two core spray systems will protect the core through a range of break sizes, without assistance from any other safeguard system, from a small break of 0.16 ft^2 thru the design basis break. With the assistance of the HPCI system, acting as a primary system depressurizer the core spray becomes effective for break down to breaks as small as 0.02 ft^2. Feedwater availability also performs this same function when AC power is available.

The core flooder injects water inside the shroud in sufficient quantities to cover the core to 2/3 of its height, thus effectively cooling it, in sufficient time after uncovery to prevent core melting. Without assistance from any of the other systems the LPCI flooder will protect the core through all break sizes from the design break down to 0.4 ft^2. With the assistance of the HPCI system (or automatic relief or the feedwater system), the flooder can protect the core down to breaks as small as 0.02 square feet. Again this is possible because either the HPCI system or the feedwater system will reduce the vessel pressure for breaks in excess of 0.02 ft^2 thereby permitting the LPCI system to make up inventory. Thus between breaks of 0.02 ft^2 and 0.4 ft^2 the core

flooder functioning in conjunction with the HPCI system will prevent clad melting. For some breaks in this range the core may uncover momentarily, but never long enough to cause clad melting.

The HPCI system injects cold water into the feedwater sparger to assure mixing and is capable of making up any inventory losses for breaks below about .02 ft^2 thus maintaining level. For break sizes between .02 ft^2 and .4 ft^2 the HPCI system acts as an effective depressurizer by virtue of its lowering the average temperature of the vessel contents, thus allowing the core spray (or core flooder system) to become effective. The pressure at which the HPCI system ceases to function, approximately 150 psig, is sufficiently overlapped by the static pressure head of the core spray pumps, 300 psig to insure that complete depressurization occurs to allow the core spray system to reach rated flow. The head of the core flooder pumps is such that they can deliver rated flow at 150 psig.

The HPCI system is backed up by automatic actuation of relief valves in terms of depressurizing the system for those break sizes which require the HPCI system. This relief action is based on simultaneous signals from high drywell pressure, reactor scram, loss of level as well as non-operation of the HPCI system. Protection can be provided to preclude inadvertent operation.

It should be noted that throughout the entire spectrum of breaks, there is at least two processes for adequately cooling the core even under conditions of no external power supply. This was the basis for satisfying the emergency core cooling design objective. However, in the more probably event of the maintenance of external power, the additional cooling provision offered by the feedwater system is also demonstrated on Figure 3. For a liquid flow break and considering the infinite water supply made available with the service water pumps as discussed in Section D of this report, all break sizes up to the approximate size of one square foot could be handled adequately by the feedwater system.

b. Breaks Above the Water Level

For all accidental breaks above the water level with no assistance from any of the other provided safeguards systems, the low pressure coolant injection system will protect the core against clad melting for the spectrum of breaks from the design breakdown to about 0.25 ft^2. With the assistance of either the HPCI system or the feedwater system acting as a depressurizer, the core can be protected against all breaks down to those as small as .06 ft^2 by the low pressure coolant injection system.

Either of the core spray systems without assistance from other systems will prevent clad melting down to breaks of 0.32 ft^2. In conjunction with the HPCI system, either core spray will prevent clad melting down to break sizes of .06 ft^2.

The HPCI system maintains the level above the core for all break sizes under about .06 ft^2 without assistance from other systems. Beyond .06 ft^2, it also acts as an effective depressurizer, thereby, permitting the low pressure coolant injection system or either of the core spray systems to become effective in adequate time to prevent fuel clad melt.

Automatic relief is a backup to the HPCI system. It permits activation of the core flooder for the break sizes for which it is required. As discussed above, with respect to the liquid flow break conditions for the more probably case of external power being maintained and therefore feedwater supply being available, an additional coolant supply is provided in excess of those required to satisfy the design objective. Figure 3 demonstrates that for the steam flow break considerations, the entire spectrum of breaks can be adequately handled by the inexhaustible feedwater supply.

4. Surveillance and Testing

To assure that the low pressure coolant injection system will function properly, if required, specific provisions are made for testing the operability and performance of the several components of the system. Testing will be done periodically at a frequency that will assure availability of the systems. In addition, surveillance features will provide continuous monitoring of the integrity of vital portions of the system.

a. Pre-Operational Testing

Prior to plant startup, a pre-operational test of the complete system will be conducted. This test will assure the proper functioning and operation of all instrumentation, pumps, heat exchangers, and valves. This test will verify that the system meets its design performance requirements. In addition, system reference characteristics, such as pressure differentials and flow rates, will be established at this time to be used as base points for check measurements in the testing to be done during plant operation.

b. Periodic Surveillance and Testing

The pumps can be started and full flow established through the bypass line back to the pressure suppression pool to determine availability of pumps and control circuits. The motor-operated valves can be exercised and their operability demonstrated. Leak tightness of the system can be demonstrated. These tests can be performed while the unit is operating or pressurized at hot standby.

When the unit is shut down and depressurized, flow rate measurements can be made with water pumped into the reactor vessel. Also, relief valves on the low pressure lines can be removed and tested for set point.

During refueling, visual inspections can be made of internal piping. Components inside the primary containment can be visually inspected when the drywell is open for access.

c. Continuous Surveillance

Pressure differential between the system piping inside the vessel and an internal reference pressure will be continuously monitored during power operation. Changes in these pressure readings will provide indication of loss of integrity of piping within the reactor vessel. Also, all pipes, pumps, heat exchangers, valves, and other working components outside of the primary containment can be visually inspected at any time.

ELECTRICAL POWER SUPPLY

The power for the low pressure coolant injection system can be supplied from the reserve auxiliary transformer, the standby diesel generator supply, or the standby transformer. It is proposed that three diesel generator units be provided and connected electrically to serve both Dresden Unit 2 and Unit 3. The diesel power supply is sized to provide required power capacity with two of the three diesel generators in operation. This capacity includes operation of required engineered safeguards and other auxiliaries for one reactor as listed below plus operation of necessary cooling equipment in the second reactor to accomplish and maintain a safe shutdown.

Equipment Automatically Placed in Service

- One Core Spray Pump
- Three Low Pressure Coolant Injection Pumps
- One Service Water Pump
- Standby Gas Treatment Equipment
- All Power Operated Valves Not on D.C.
- Emergency A-C Lighting
- Instrumentation and Control Motor-Generator

D. FEEDWATER SYSTEM INVENTORY UNDER ACCIDENT CONDITIONS

Detailed evaluations of the feedwater system have indicated that the potential inventory which can be pumped into the reactor under loss-of-coolant conditions is unlimited. In preliminary discussions with respect to the potential inventory offered by the feedwater system under accident conditions, a claim of only approximately four minutes of rated feedwater flow from the condenser hotwell was available. The detailed evaluations have indicated that the hotwell of the main condenser can be, and has been, provided with a water supply of as much as 18,000 gpm by any one of the three service water pumps. Therefore, with a full feedwater flow from the hotwell to the reactor occurring, an acceptable hotwell water level could be maintained for continuous feedwater flow. The influence of this additional coolant supply under accident conditions is demonstrated in Figure 3.

E. EXCURSION ANALYSIS

An examination has been made of the possible uncertainty in the excursion analysis for a 2.5 percent ΔK rod dropped at 5 ft/sec in order to evaluate the hydrodynamic effects on the core internals.

The uncertainties in the physics calculations are presented in Dresden Unit 3 Plant Design and Analysis Report, Amendment Number 3. The resulting peak fuel enthalpy is 210 ± 20 calories per gram. This value is slightly below the incipient melting point of the fuel.

Experimental data[1] are available which indicates that the fuel rods will retain their integrity after super-prompt critical excursions which raise the fuel temperature to the incipient melting point.

Assuming integral fuel pins, the maximum heat flux to the water in the hottest channel is less than twice the heat flux of the hot channel for normal power operation. This ratio (hot channel power, accident)/(hot channel power, normal) drops to unity approximately five seconds later.

This value of heat flux is limited to four channels and will result in some film boiling (also observed in the Pulstar tests), but no gross clad melting is expected. This results in channel exit flow velocities which would not result in distortion or damage to the core spray spargers or to the vessel-shroud configuration.

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[1] "Summary Report, Pulstar Pulse Tests - II," WNY-020, February 1969.

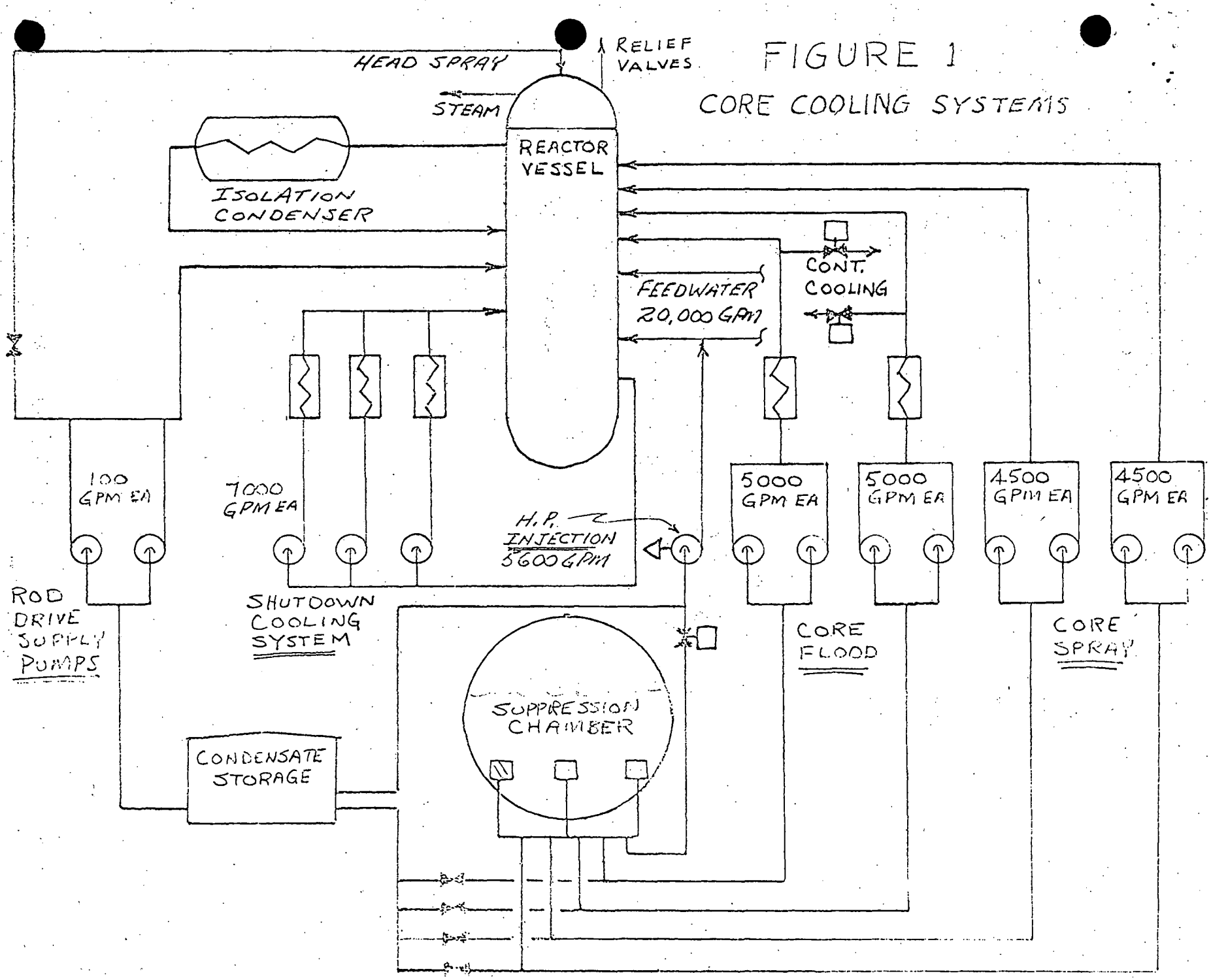

FIGURE 1

CORE COOLING SYSTEMS

GENERAL ELECTRIC

UNLESS OTHERWISE SPECIFIED USE THE FOLLOWING:—					REV NO.
APPLIED PRACTICES	SURFACES	TOLERANCES ON MACHINED DIMENSIONS			
		FRACTIONS	DECIMALS	ANGLES	
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TITLE FIGURE 2
LOW PRESSURE COOLANT INJECTION

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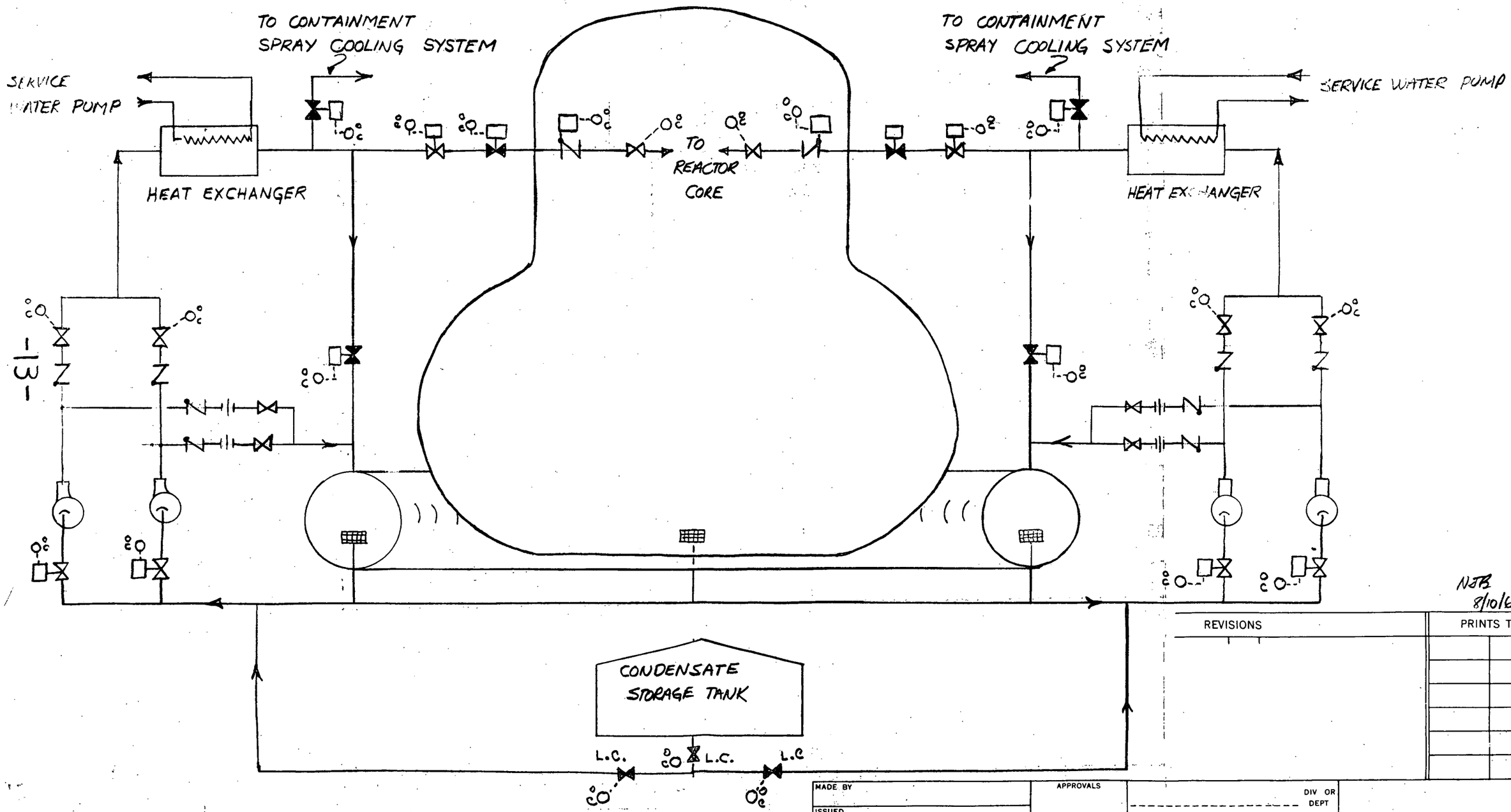

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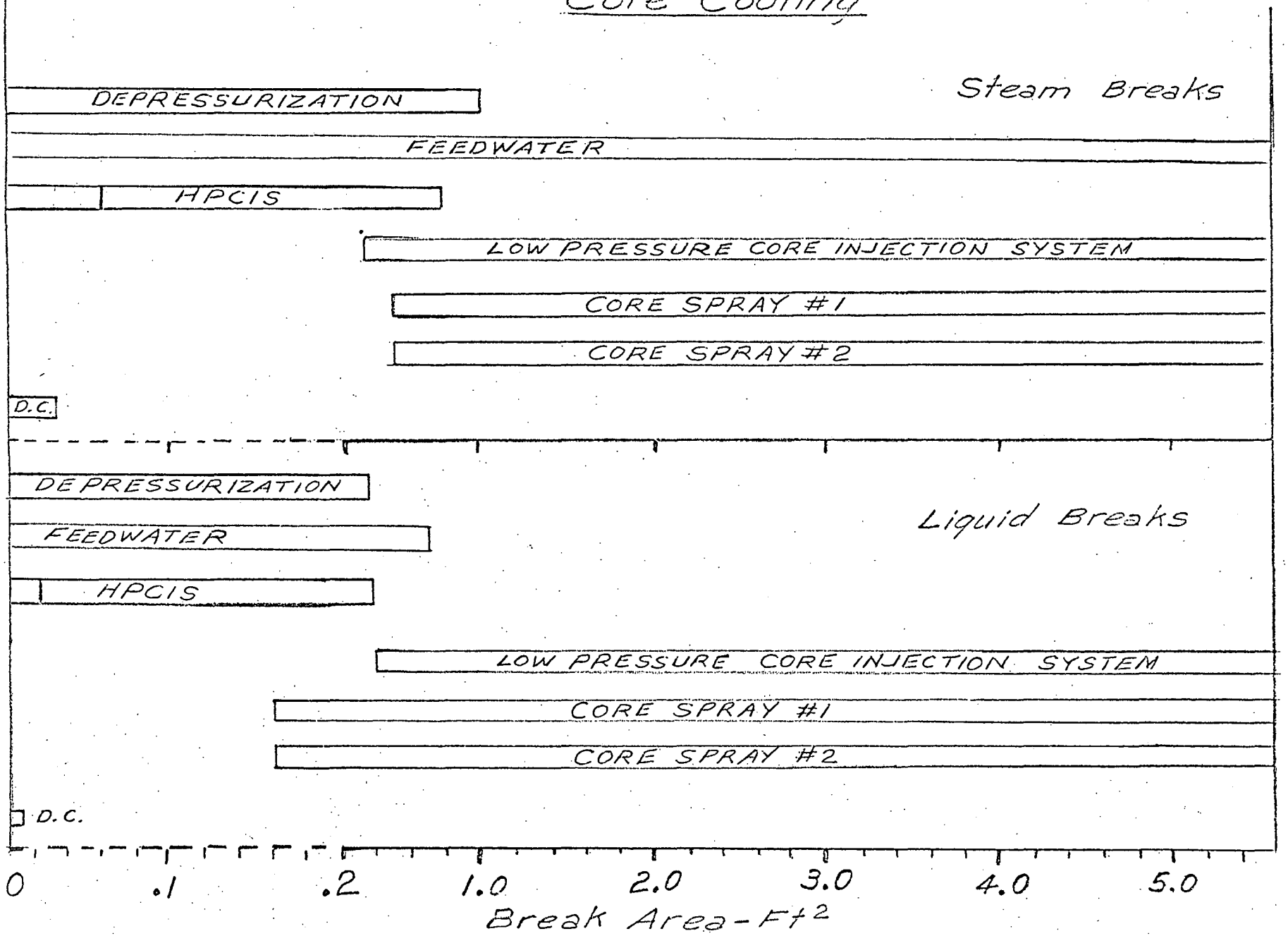
FIGURE

Dresden III Integrated Performance
Core Cooling



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DESCRIPTION AND EVALUATION

OF DRESDEN UNIT 3

EMERGENCY CORE COOLING PROVISIONS

July 26, 1966

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ERRATA

Page Number	Line	Comment
II.2.1-1	12	230 becomes 250
II.2.1-4	11	Delete phrase "and reactor water level"
II.3.3-4	17	Table 1 becomes II.3.2-1
II.3.3-14	7	Figure 16 becomes II.3.3-8
II.3.6-2	24	4500 becomes 4550
II.3.9-4	3	20 becomes 30
II.3.10-1	8	300 becomes 260
II.4-2	18	Section II-B becomes Section II.2
III-3-6a		Section Heading III.3.6 becomes III.3.7
APPENDIX III.A		RCIC becomes HPCI throughout

I. INTRODUCTION

1. Continuity of Core Cooling

As a guiding principal the General Electric Boiling Water Reactor is designed to provide continuity of core cooling for the entire range of anticipated operating conditions and for a wide variety of potential yet highly improbable incidents.

During power operation of the reactor, heat is removed from the core through the generation of steam which is subsequently dissipated as mechanical energy and thermal energy in the turbine-generator and the turbine main condenser. Heat is also removed by the main condenser when the reactor is pressurized and is starting up or shutting down. When the reactor is shutdown and depressurized, the reactor decay heat is removed by the shutdown cooling system. These systems provide for continuous core cooling under normal operational circumstances when electrical power is available.

Provisions are also made to dissipate heat generated in the core and to assure adequate fuel cooling under circumstances when the reactor is isolated from the main condenser and the shutdown cooling system, or when no electrical power is available to pump cooling water to the main condensers or the shutdown cooling heat exchangers. Such provisions for cooling are accomplished at Dresden Unit 3 by means of the isolation condenser with overlapping backup by relief valve action in conjunction with the posed high pressure coolant injection system.

Further core cooling systems are provided for those postulated conditions wherein it is assumed that mechanical failures occur and coolant is partially or completely lost from the reactor vessel. This is accomplished by means of the core spray cooling systems and the high pressure coolant injection system. These systems are discussed in detail in the remainder of

this report as the provisions for safeguards against coolant loss accidents.

It is apparent that a multiplicity of systems is provided, each designed to cover a particular range of conditions. Continuity of cooling is assured by appropriate operational overlap between particular systems. It is intended to demonstrate that for all modes of intentional or accidental reactor operation, there is available at least two systems for providing adequate core cooling.

I.2 Principal Performance Objectives for Safeguards Against Coolant Loss Accidents

The principal performance objectives and design requirements for the combined cooling systems for the various circumstances of coolant loss accidents are as follows:

a. To assure continuity of core cooling over the entire spectrum of coolant loss indidents from a small leak up to and including the total loss of normal reactor coolant inventory from the reactor vessel brought about by the design basis accident.

b. To provide redundancy of equipment such that a single failure of active components and simultaneous failure of singular static components will not render the core cooling systems ineffective.

The design bases and specific requirements of the various proposed systems are discussed in subsequent sections hereof.

I.3 Scope of Contents

It is the purpose of this report to provide a thorough assessment of the mechanical systems to be provided in Dresden Unit 3 as safeguards against coolant loss accidents. The format of presentation is similar to that found in the Plant Design and Analysis Report, and consists of the following:

a. Design basis for each safeguards systems.

b. Description of the system designs

c. Design evaluation of each system

d. Proposed methods of surveillance and testing of each system

In addition, this report describes the interrelations and overlap between the systems which demonstrate that the integrated systems provide the required cooling continuity.

It is anticipated that the technical content of this report will be utilized as a basis for modifying appropriate sections of the Plant Design and Analysis Report for Dresden Unit 3.

I.4 Summary of Changes

a. Core Spray System

1. Add redundancy of pumps
2. Add redundancy of active valves
3. Increase system design pressure

b. High Pressure Coolant Injection System

1. Add entire system

c. Isolation Condenser

1. Delete one system

d. Electro-matic Relief Valves

1. Add reset control in control room

II CORE SPRAY SYSTEM

II. 1. Design Basis

The following Design Basis has been adopted for the Core Spray System and has therefore served as the basis for evaluating the adequacy of the provided system.

1. Core spray systems are provided to prevent any fuel melting as a result of the various postulated but improbable loss of primary coolant accidents for a range of sizes from below those for which adequate protection is offered by the High Pressure Coolant Injection System up to and including the design basis accidnet - the instantaneous mechanical failure of size equal to the largest primary system pipe.

2. The core spray systems shall be provided in such a manner as to have two independent full capacity systems in which each system shall have redundancy of active components.

3. The core spray system shall meet the above design basis requirements without reliance on external power supplies to the core spray or the reactor system.

4. The core spray system shall be designed so that each component of the system can be tested on a periodic basis.

II.2 - System Design

II.2.1 Description

The core spray system is designed to pump water directly from the suppression pool into the reactor vessel under loss of coolant conditions associated with large pipe breaks and reactor vessel depressurization.

Two independent core spray systems are provided. Each system consists of pumps, piping, valves, instrumentation and a spray ring sparger of sufficient capacity to provide core cooling. One redundant pump is provided in each system to assure pump availability. Redundancy has also been incorporated in those valves which must be activated during initiation of the core spray systems to assure coolant delivery. The piping and instrumentation diagram for the system is shown in Figure II.2-1. The design flow capacity of each pump in each core spray system is approximately 4550 gpm at a total developed pump head of 230 psig, as shown in Figure II.2-2. The power required for each pump is approximately 1200 HP. The water source for the pump suction is the 800,000 gallon suppression pool. The condensate storage tank water is used for initial flushing and periodic testing of the system.

The specification of the flow rate is based on refined prototype testing of a full scale fuel bundle under actual power conditions and actual spray distribution conditions. In order to insure that the test situations resulted in a limiting case, the test fuel rods were allowed to overheat (1250°F) prior to core spray activation and the channel boxes were allowed to stay at high temperature. The spray systems have been sized to provide the maximum required flow rate to each fuel bundle in the core. Flow distribution in the upper plenum as well as leakage flow available to fuel rods were also taken into account in establishing the flow requirements.

Each core spray pump supplies water to a spray sparger located inside the core shroud of the reactor vessel directly above the core. Water is sprayed directly onto the top of fuel assemblies in a pre-established pattern through approximately 130, one inch diameter spray nozzles located in the sparger. Internal piping which connects the spargers to the reactor pressure vessel penetration, is designed and routed to meet the necessary flexibility requirements for thermal expansion.

Design of the piping system external to the reactor vessel includes considerations for potential damage to this piping system. The pipe runs of each system are physically separated and located to take maximum advantage of protection afforded by structural beams and columns. A sketch of pipe

protection provisions is shown in Figure II.2-3 Drywell penetrations for the core spray pipes are located to achieve minimum pipe runs within the drywell, providing maximum circumfirutal distance between main steam and feedwater lines as shown on Figure II.2-4.

The core spray pumps are located in the lower-most corners of the reactor building. Physical separation of the pumps of each system is achieved by locating pumps in different corners as shown by Figure II.2-5. Suction water from the pressure suppression pool to the pumps is taken from a common ring header that has three suction stainless steel screens located in the torus. These screens are positioned such that they are some distance above the bottom of the suppression pool and well below the pool surface to minimize plugging from debris. Sufficient flow area is available to provide several times the flow requirements of the combined use of both core spray systems, and the High Pressure Injection Cooling System. Screen size (1/8" openings) has been selected to screen out particles capable of plugging spray nozzles.

The piping system is fabricated of carbon steel from the suppression chamber to the outer isolation valve. Safety valves are utilized for pressure protection of this section of the system. From the outer isolation valve into the reactor, the system is fabricated of stainless steel and designed for service at 1,250 psig and 575°F. The spray spargers and spray nozzles are fabricated from 304 stainless steel to ASME, Section III Code. The core structure supporting the spray spargers is also fabricated of 304 stainless steel material. The vessel nozzle entry material is Ni Cr Mo forging fabricated to ASME SA336 and modified by ASME Code Case 1332.

The most severe environmental conditions that the isolation valves of the core spray systems are expected to encounter is a postulated event in which a piping failure releases a mixture of steam and water within the drywell. Less than 30 seconds after the break, the drywell pressure will equalize to about 21 psig. The maximum ambient temperature of the isolation valves following this transient is expected to be less than the drywell design temperature of 281°F.

The pumps and motors associated with the core spray systems are located in the lower corners of the reactor building, where maximum ambient conditions are estimated to be 150°F at a relative humidity of 100%.

The power supply for the core spray system is located on a separate emergency bus that has provisions for control of adverse environments such as could be caused by fire or steam line breaks. Power for the emergency bus can be supplied from the reserve auxiliary transformer, standby diesel generator, or the standby transformer. The core spray systems are automatically actuated by the reactor protection system upon receipt of a low-low water level signal in the reactor vessel. They can also be manually activated from the control room. A test line capable of full system flow is connected from the outside isolation valve back to the torus. Flow can be diverted into this line to test operability of the pumps and control system during reactor operation.

The control system is arranged to provide two independent and separately isolated control and power circuits for operation of the two independent pumping loops, as shown in Figure II.2-6.

II.2.2 System Control

II.2.2.1 Core Spray Initiation with Plant on Normal Auxiliary AC Power

1. Initiation occurs from reactor low-low water level indication detected by four independent level sensing switches A, B, C, D, connected in one out of two twice array.
2. The low-low water initiation signal starts one pump in each loop and signlas the startup valves MO1-16 A (B) and MO2 - 16 A (B) in both loops to open. These valves open only after reactor pressure decays below pump discharge pressure, at which time the permissive signal to open the valves is initiated by two pressure switches in each system connected in parallel so that a low pressure indication from either switch will actuate the valves.
3. In the event a pump fails to deliver flow within a short time after the valves start to open, the full capacity redundant pump will be automatically started.
4. The system response time is estimated to be as follows:
   (a) 3 seconds for sensing low-low water level and initiation of the start signals.
   (b) 5 seconds for the pump to accelerate to full speed.
   (c) Reactor pressure decay plus 3 seconds for startup valves to allow measurable flow.
   (d) 20 seconds for startup valves to reach full opening after opening signal is received.

5. The Core Spray low-low level initiation signal shall signal the emergency diesel generator to start.

II.2.2.2 Core Spray Initiation With Plant on Standby AC Power

1. Initiation by reactor low-low water level as above.
2. One pump in one of the two loops will automatically start. In event of failure of that pump its alternate will start automatically. In event of failure of the alternate pump the core spray initiation signal will pass to the second loop where the automatic start-up procedure will repeat.
3. Automatic opening of the start-up valves is initiated by low-low reactor water level and reactor water level and reactor low pressure as above.

II 2.2.3 Core Spray Initiation for System Testing

The system may be tested during reactor operation as follows:
(Assume System I selected for test)

1. When a loop is to be tested, the other loop is placed on auto-start priority by the system selector switch.
2. The pump of the loop under test may be started by its manual control switch. The test by-pass valve is opened to allow the pump to be tested. at full flow. Flow and pressure instrumentation is observed for correct response and the system outside the drywell may be checked for leaks.
3. The start-up valves and the testable, check-isolation valves may be tested independently of the pump and flow test as follows:
   a. Maintenance valve MO. 1-15 is closed by the control switch. Limit switches on the maintenance valve MO. 1-15 acts as permissive to open the start-up valves MO. 1-16A and MO. 1-16B. The latter valves may be exercised opened and closed by manual actuation of the control switches for each valve.
   b. With valve MO. 1-16A and MO. 1-16B fully closed, valve MO. 1-15 must be reopened at the end of the test.
   c. The testable check valves 1-20A and 1-20B may be stroked, opened, and closed by manual actuation of the control switches during any reactor operating status.

II 2.2.3.1 Loss of Coolant Accident Concurrent With Test

In event low-low water occurs during a loop test, the stand-by loop not under test will start automatically. The loop being tested will return automatically to stand-by readiness and will start automatically if the plant is on normal AC power or in event of a failure of the other loop.

II 2.3 Design Provisions to Test Component Performance

Provisions have been designed into the core spray system to test the performance of its various components, described as follows:

2.3.1 Instrumentation

Plant per-operational test of entire system.
Periodic system tests using test lines.

2.3.2 Valves

Plant pre-operational test of entire system
Periodic system tests using test lines.
Leak-off lines between isolation valves.
Drainline on pump side of outboard isolation valves.
Safety valves can be removed and tested for set point.
Motor-valves can be exercised independently.

2.3.3 Pumps

Plant pre-operational test of entire system.
Periodic system test lines.
Pump seal leakage is monitored.

2.3.4 Spray Sparger

Plant pre-operational test of entire system.

2.3.5 Spray Nozzles

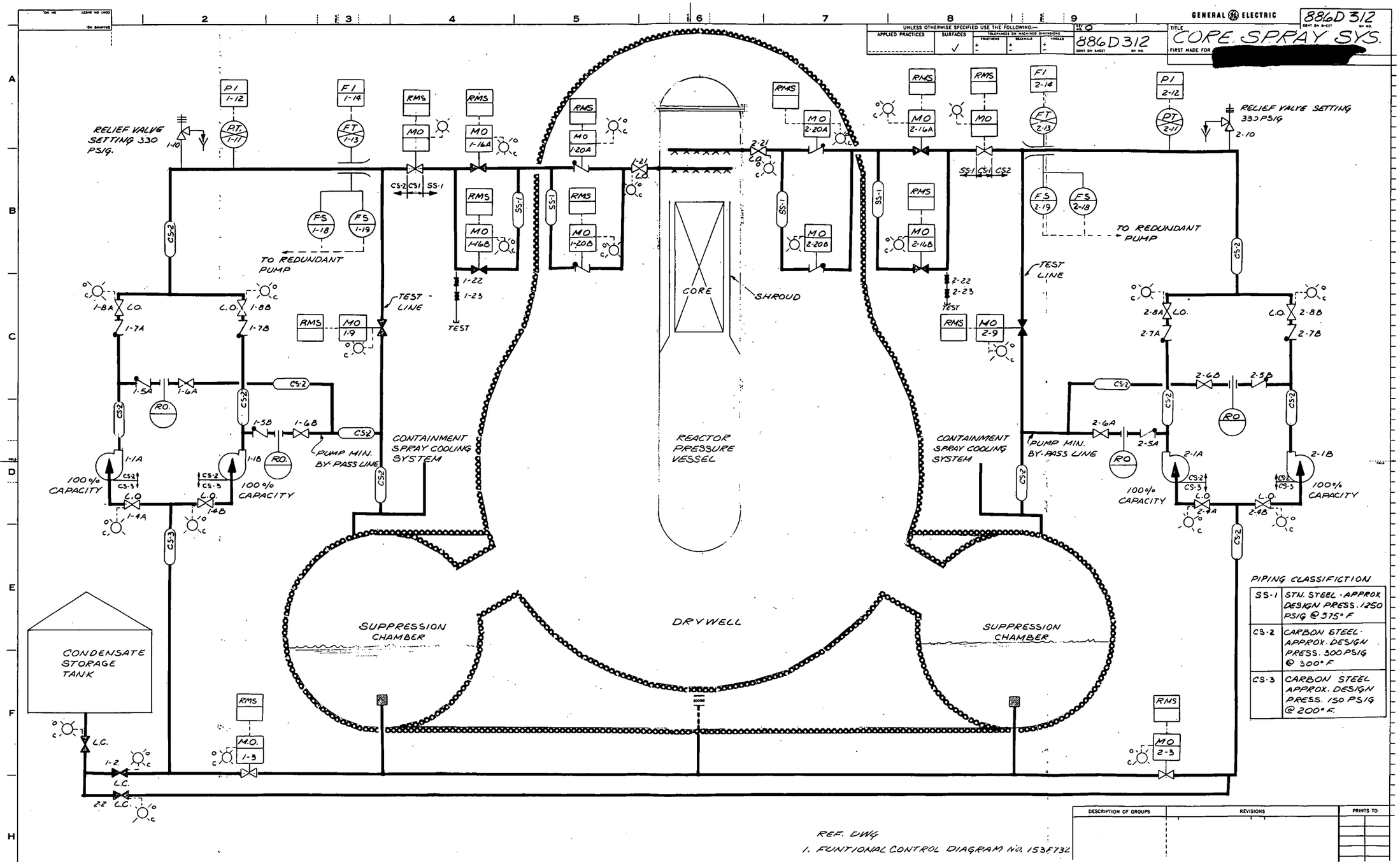
Plant pre-operational test of entire system.

2.3.6 Relief Valve

Can be removed and tested for set point.

2.3.7 Screens

Plant pre-operational test of entire system.
Periodic system test using test lines.
Pressure indicator or pump suction during above tests.


GENERAL ELECTRIC
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FIRST MADE FOR [redacted]
RELIEF VALVE SETTING 330 PSIG.
RELIEF VALVE SETTING 330 PSIG
TO REDUNDANT PUMP
TEST LINE
TEST
CORE
SHROUD
REACTOR PRESSURE VESSEL
CONTAINMENT SPRAY COOLING SYSTEM
PUMP MIN. BY-PASS LINE
100% CAPACITY
SUPPRESSION CHAMBER
DRYWELL
CONDENSATE STORAGE TANK
PIPING CLASSIFICTION
SS-1 STN. STEEL - APPROX. DESIGN PRESS. 1250 PSIG @ 575° F
CS-2 CARBON STEEL - APPROX. DESIGN PRESS. 300 PSIG @ 300° F
CS-3 CARBON STEEL APPROX. DESIGN PRESS. 150 PSIG @ 200° F.
REF. DWG
1. FUNTIONAL CONTROL DIAGRAM No. 153F732
DESCRIPTION OF GROUPS
REVISIONS
PRINTS TO

FIGURE II.2-1

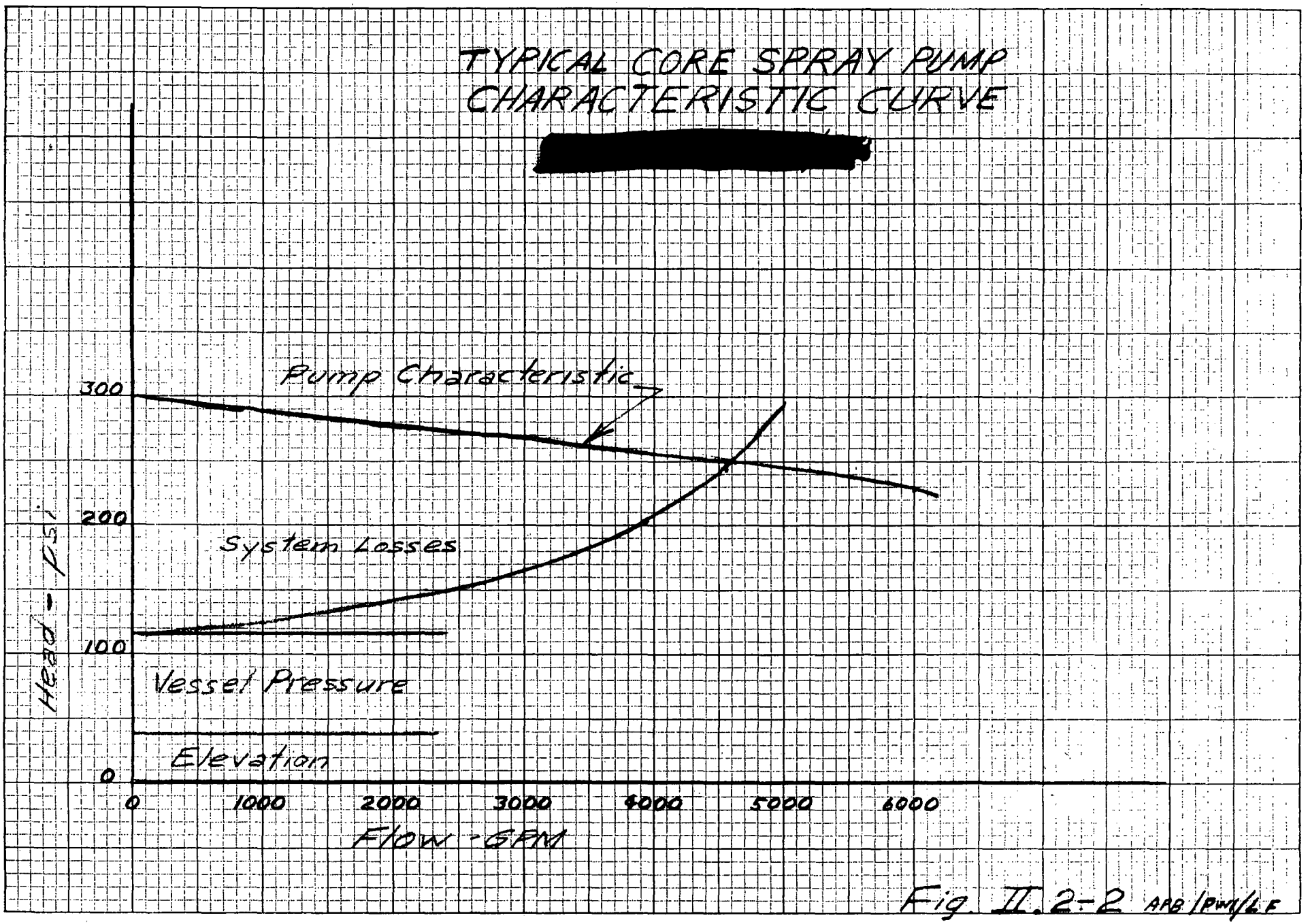


Fig. II.2-2 ARB/PWH/LF

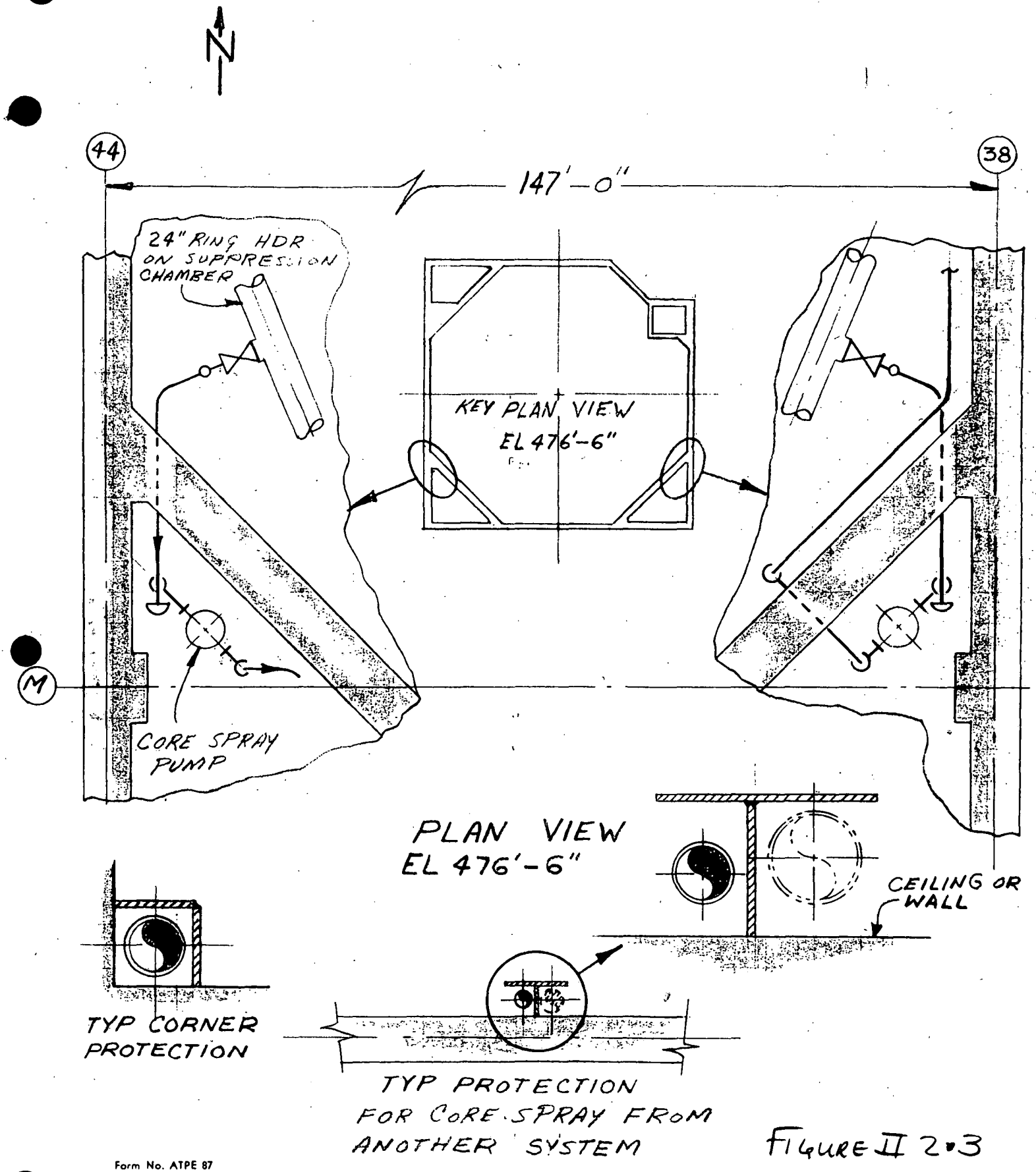


FIGURE II 2.3

Form No. ATPE 87

GENERAL ELECTRIC

SK-56121-921

UNLESS OTHERWISE SPECIFIED USE THE FOLLOWING—

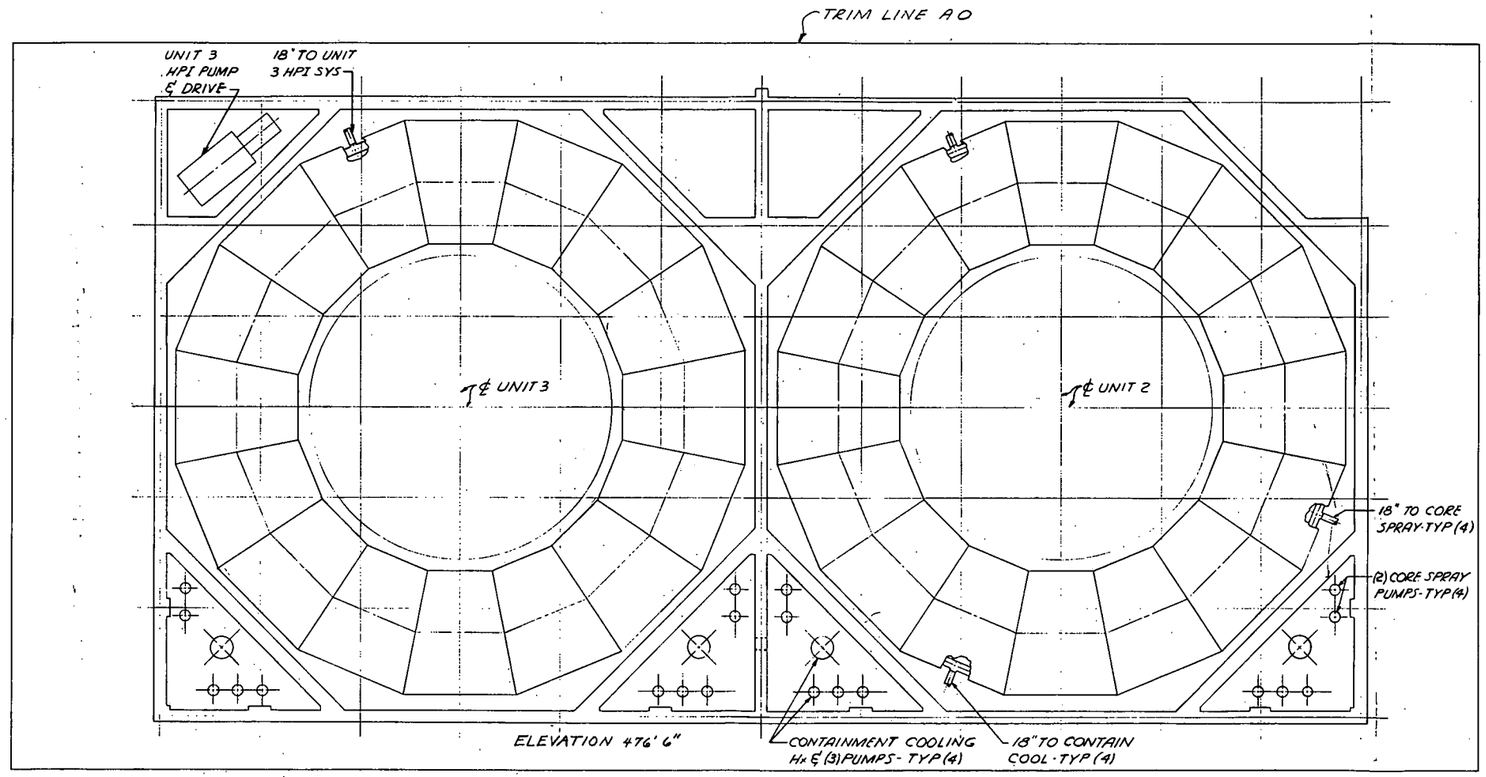
APPLIED PRACTICES	SURFACES	TOLERANCES ON MACHINED DIMENSIONS: FRACTIONS	DECIMALS	ANGLES
	✓	±	±	±

REV. NO. Q

SK-56121-921L

TITLE CONCEPT ADDITION OF HPI SYSTEM

FIRST MADE FOR



II-2-5

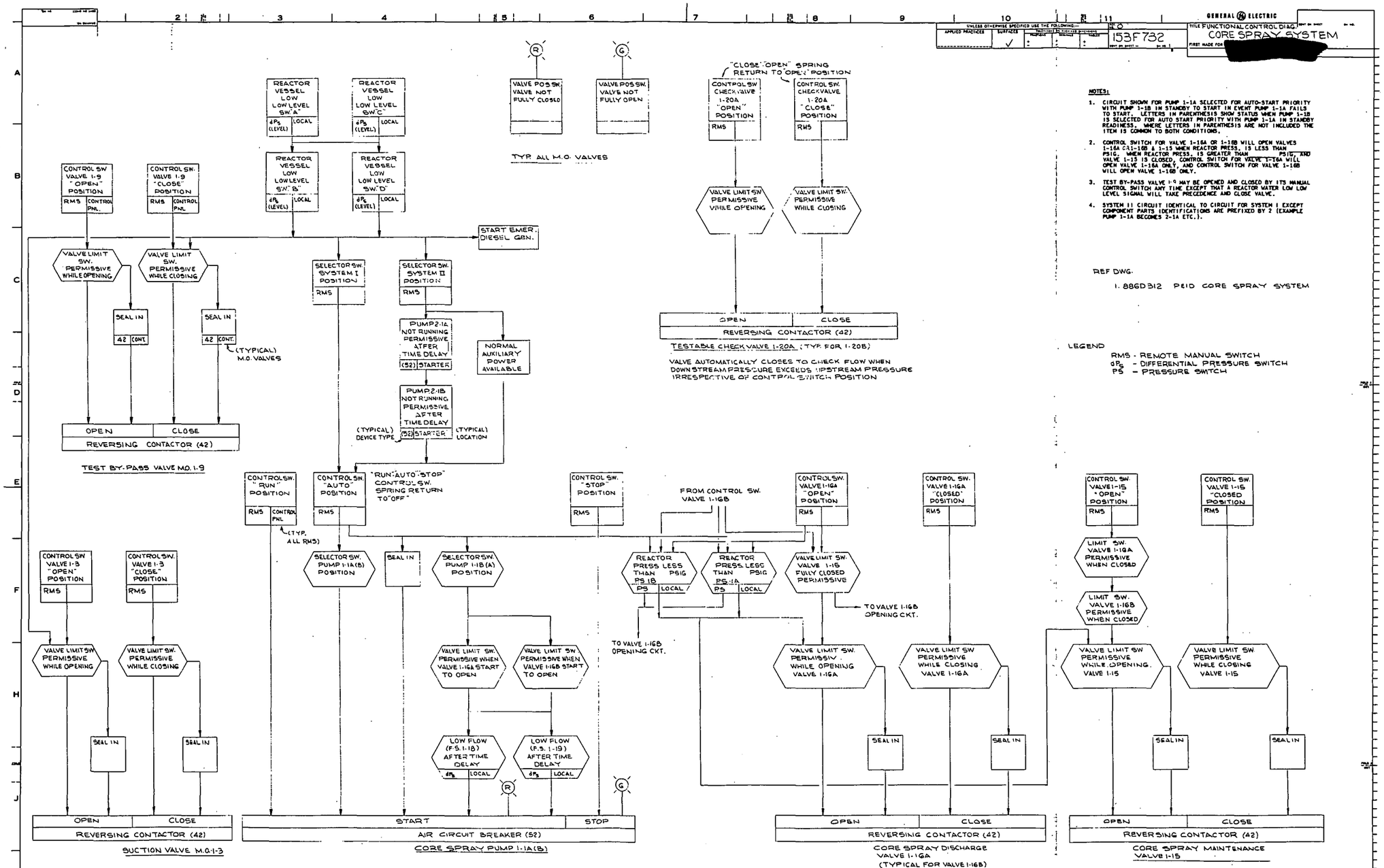
DESCRIPTION OF GROUPS	REVISIONS	PRINTS TO

FIGURE II. 2-5

GENERAL ELECTRIC

TITLE FUNCTIONAL CONTROL DIAG.
CORE SPRAY SYSTEM
FIRST MADE FOR [redacted]

153F732



NOTES:

1. CIRCUIT SHOWN FOR PUMP 1-1A SELECTED FOR AUTO-START PRIORITY WITH PUMP 1-1B IN STANDBY TO START IN EVENT PUMP 1-1A FAILS TO START. LETTERS IN PARENTHESIS SHOW STATUS WHEN PUMP 1-1B IS SELECTED FOR AUTO START PRIORITY WITH PUMP 1-1A IN STANDBY READINESS. WHERE LETTERS IN PARENTHESIS ARE NOT INCLUDED THE ITEM IS COMMON TO BOTH CONDITIONS.
2. CONTROL SWITCH FOR VALVE 1-16A OR 1-16B WILL OPEN VALVES 1-16A (1-16B & 1-15 WHEN REACTOR PRESS. IS LESS THAN PSIG. WHEN REACTOR PRESS. IS GREATER THAN PSIG, AND VALVE 1-15 IS CLOSED, CONTROL SWITCH FOR VALVE 1-16A WILL OPEN VALVE 1-16A ONLY, AND CONTROL SWITCH FOR VALVE 1-16B WILL OPEN VALVE 1-16B ONLY.
3. TEST BY-PASS VALVE 1-9 MAY BE OPENED AND CLOSED BY ITS MANUAL CONTROL SWITCH ANY TIME EXCEPT THAT A REACTOR WATER LOW LOW LEVEL SIGNAL WILL TAKE PRECEDENCE AND CLOSE VALVE.
4. SYSTEM II CIRCUIT IDENTICAL TO CIRCUIT FOR SYSTEM I EXCEPT COMPONENT PARTS IDENTIFICATIONS ARE PREFIXED BY 2 (EXAMPLE PUMP 1-1A BECOMES 2-1A ETC.).

REF DWG.

1. 886D312 P&ID CORE SPRAY SYSTEM

LEGEND

RMS - REMOTE MANUAL SWITCH
dP_S - DIFFERENTIAL PRESSURE SWITCH
PS - PRESSURE SWITCH

PRELIMINARY

FIGURE II.2-6

II.3 - CORE SPRAY SYSTEM EVALUATION

II.3.1 Introduction

In evaluating the core spray system, the important performance objectives which must be considered to show conformance with the design objectives, stated in Section II.1, are as follows:

a. The system must have a high degree of reliability.

b. The integrity of all structures necessary for the spray flow must be insured following the design basis accident.

c. The quantity of flow delivered must be adequate and properly distributed across the core.

d. The spray flow entering the fuel assemblies must be effective as a cooling agent.

e. The required flow must be delivered in sufficient time to meet the design objectives even in the postulated absence of an external AC power source.

In the following sections of this report it will be shown that each of the above performance objectives and hence the design bases have been met, on the basis of systems performance analyses coupled where necessary, to experimental data.

II. 3.3 STRUCTURAL INTEGRITY OF ESSENTIAL COMPONENTS

3.3.1 Analysis of Forces on Reactor Internals

Following rupture of the primary system, internal pressure forces result across the various reactor components some of which may exceed the forces existing during normal operation. These dynamic pressure forces acting on each of the major internal reactor components during various abnormal flow conditions are calculated with a model in which the reactor is represented by a series of constant volume nodes interconnected by two phase flow resistances. Each node is assumed to be in the thermodynamic equilibrium and the equations for conservation of mass, energy and momentum are applied to calculate pressure response as a function of time. The node may be subcooled liquid or saturated liquid and/or vapor. Figure II. 3.3-1 illustrates the basic pressure nodes (volumes) of the model. This model is described in detail in Appendix IIA along with a comparison with some existing experimental data of a small scale mock-up of a reactor vessel and its internals.

Recent publications in the literature in connection with the LOFT Program indicate the possibility of the existence of very large forces across reactor components following a primary system rupture. However the analyses and experiments reported are not for boiling water reactors. These publications relate to water reactors having a high degree of subcooling

throughout most of the volume and do not have a steam dome within the pressure vessel. These are important parameters in limiting the magnitude of the transient internal pressure differences for BWR's.

The pressure forces acting on the reactor internals are designated as follows:

Dryer	P_2-P_1
Separator	P_3-P_2
Upper Shroud	P_3-P_2
Channel Box	P_4-P_3
Core Plate	P_5-P_3
Shroud Support	P_5-P_2

These forces have been calculated for each of four cases of abnormal flow conditions; 1) double ended steam line break, 2) double ended recirculation line break, 3) hot standby control rod drop, 4) break of the 2" diameter line in the lower plenum. A pressure vessel rupture above the water line is represented by case 1 since dryer pressure drop is small for single phase flow. A pressure vessel rupture below the water line outside the shroud is represented by case 2.

Case 1 -- Steam Line Break

The steam line break assumes the break to be upstream of the flow limiter since this is the worst case. Critical flow is established in one full line area plus the flow limiter area on the other end of the break which is fed by the three remaining lines. Maximum pressure forces usually occur during the first five seconds and during this time the recirculation pumps will be operating at full flow since the vessel pressure drop to cause cavitation at the pump suction is not reached until that time. Also maximum pressure differentials occur with the pumps running since the pumps maintain the lower plenum pressure.

Due to depressurization of node 2 and the resulting flashing, the water level rises in node 2 and strikes the dryers in roughly four seconds. At this time two phase flow through the dryers is initiated and the total mass flow rate through the dryers increases although the steam mass flow drops. Since steam is still flowing through the steam line while incoming steam flow has been reduced, the pressure in node 1 drops as shown in Figure II 3.3-2. At 4.5 seconds when the two phase flow reaches the steam line, exit steam flow is reduced and node 1 pressure begins to recover. At this time pressure forces are reduced as seen in Figure II 3.3-3. Maximum pressures acting during this time are summarized in Table II.3.2-1

Case 2 -- Recirculation Line Break

The recirculation line break is assumed to occur at the pressure vessel outlet. For the double ended break, critical flow is established through the break at the pressure vessel and, in addition, through the 10 jet nozzles at the other end of the recirculation line. The other 10 jet pumps operating from the second centrifugal pump are assumed to continue full flow operation. The critical flow rate from the break is evaluated using the node-to-ambient pressure drop and the flow friction of the line. The steam line isolation valve is assumed to begin closing at 3.5 seconds and is fully closed at 6.5 seconds. Flow is permitted from the lower plenum to the downcomer through the inoperative jet pump diffusers.

The effect of the recirculation line break on internal pressure is shown in Figure II 3.3-4. Initially the lower plenum depressurizes to saturation, at which time the pressure rate slows due to the compressibility of the steam formed. The pressure forces acting during this break are shown in Figure II 3.3-5 and the maximum values are tabulated in Table 1. Although high flow rates exist through the break area, the compressibility of the steam over the downcomers tends to hold the pressure up and prevents the high pressure forces due to sudden depressurization.

Case 3 -- Hot Standby Control Rod Drop

For the hot standby case, the pressure vessel is assumed to be isolated with the feedwater valve closed, the steamline isolation valve closed and the recirculation pumps in operation. Any pressure rise due to a control rod drop is to be contained within the pressure vessel. The pressure vessel is assumed to be filled to the normal water level with saturated water. The power pulse due to a control rod drop is shown in Figure II 3.3-6.

The rise in pressure due to control rod drop is shown to be slight. Assuming the pressure vessel is initially at 1015 psia, the maximum core pressure is 1021 psia. The pressure forces acting in the pressure vessel are shown in Figure II 3.3-7 and the maximum values are given in Table 1.

Case 4 - Lower Plenum Line

Large pressure forces are not developed during a two inch line break in the lower plenum. The flow out the break is very small compared to the total recirculation flow. The recirculation flow through the core would be reduced but rapid depressurization of the lower plenum would not occur. The two inch line represents the largest pipe below the core shroud for which a break is credible.

II 3.3.2 Structural Analysis

II.3.2.1 General Design Philosophy for Core Structure Internals

The entire reactor internal structure including the core spray system and associated piping is designed to remain within the stress requirements of the ASME Boiler and Pressure Vessel Code Section III for Class A reactor pressure vessels for all steady state and routine operating transients. Deflections are also examined and limited so that the normal functions of the components will not be impared.

For non-routine transients such as occurs in a steam line break or a loss of coolant accident the core spray piping and feedwater sparger are designed to remain within the stress requirements stated above.

The remainder of the core structural components which are essential for core spray operation are designed to withstand all the resulting primary stresses within the requirements of the ASME Code Section III for Class A vessels. Core components which are deemed essential to core spray operation include the core shroud, shroud support, core plate and top guide. Secondary stresses which may be induced in some of the massive structures above are examined to insure that structural integrity is maintained, that is, the performance of the core spray is in no way affected by the resultant stresses on the core structure.

To illustrate the above design philosophy the following table summarizes the differential pressures calculated in the previous section and taken into account in the design of the core structure components along with the accident loading:

Table II 3.2-1

Pressure Loading, PSI

		ASME CODE		LINE SEVERANCE	
	Normal	Design Basis	Capability	Steam	Recirc.
Spray Sparger & Internal Pipe	8/ 107**	250*	1000	16	8
Upper Shroud	8	25*	185	16	8
Core Plate	14	45	53	38	14
Guide Tubes	14	45	68	38	14
Lower Shroud	22	70*	185	54	22
Shroud Support	22	100	100	54	22

* Stiffness or mechanical strength are more limiting than pressure.
**With core spray system in operation.
Note that the pressures used for design basis in all cases exceed both the accident pressures and the normal operating pressures.

In the unlikely event of a loss of coolant accident or a steam line break, thermal gradients may be developed such that gross plastic deformations will occur. In this case, the structure is examined to determine if the structural integrity is affected. The maximum plastic deformation in the core structure was found to occur in the shroud support in the ligament between the jet pumps. The deformation was such to induce 7% strain in this region. It is to be emphasized that this strain is localized and that the ability of the shroud support to support the shroud and core spray system is in no way impared by this deformation.

B. Detailed Discussion on Design Philosophy of the Core Spray and the High Pressure Coolant Injection System

Core Spray and Feedwater Nozzles.

The core spray and feedwater sparger nozzles as well as all the reactor vessel nozzles are designed to all the stress requirements of the latest revision of Section III ASME Code Class A Vessels.

Transient and steady state requirements in the form of temperatures, pressures, nozzle reactions, and numbers of cycles are specified to the reactor vessel vendor in the form of issued drawings. Resulting stress calculations from the above input arecomputed by the vessel vendor and submitted to the General Electric Company for approval.

The calculations are checked for accuracy of the analytical model by carefully reviewing assumptions and for numerical accuracy by comparison with calculations performed on previous reactor vessels. In the event of a new design, the calculation will also be preformed by GE if quick checks guaranteeing the accuracy of the analysis cannot be performed.

The core spray piping is welded on to its thermal sleeve which in turn welded to the vessel nozzle. Ten cycles of core spray operation are examined for the fatique requirement of Section III of the ASME Code.

The feedwater sleeve is not welded into the nozzle but slips into the nozzle with a close tolerance fit such that leakage between the vessel nozzle and the feedwater sparger sleeve is controlled to 200 gpm for a twenty-five psi differential pressure. Among the cyclic loadings applied to the nozzle are 130 temperature steps where cold water is injected against the hot nozzle and thermal sleeve.

Core Spray Piping and Sparger.

The core spray piping external to the shroud is analyzed taking into account the differential thermal expansion between the shroud and vessel for normal operating condition and routine maneuvering transients. The effects of cooling the core structure components quicker than the reactor vessel following a loss of coolant accident are also taken into account. The conditions analyzed are:

1. Normal operation with the reactor vessel and internal at 545°F.
2. Core spray initiation with the vessel and structure assumed at 545°F and the piping at 70°F.
3. Core spray cooling with the vessel at 545°F and the piping and core structure at 135°F, the temperature of the torus water after a loss-of-coolant accident.

The combination of these stresses and stress due to internal pressure is in all cases within the ASME Code allowable.

Additionally, the deflections induced by earthquake loading are analyzed to insure that any differential movement will not overstress or impair the function of the piping.

The core spray spargers inside the shroud are analyzed assuming the shroud to be at 545°F and the spargers at 70°F. These spargers are mounted in brackets which allow them to contract circumferentially and the stresses are well within Section III allowable.

The core spray piping and sparger are arranged to minimize the potential for vibration. The spray feed pipe between the vessel and shroud is run

Core Spray Piping and Sparger (Cont'd).

underneath the feedwater sparger and is mounted approximately four feet above the annulus between the shroud and vessel. The purpose is to put it in a low velocity region consequently minimizing any tendancy for hydraulically induced vortex shedding vibration. In addition, brackets mounted to the vessel wall are provided to stiffen the pipe system to further minimize the possibility of any hydraulically induced vibration.

Each sparger inside the shroud is divided into two 180° sections, each section is mounted to the inside shroud surface at 6 locations. The sparger is mounted in brackets which permit tangential motion in the event the core spray is turned on to permit relative motion between the sparger and shroud. However, the sparger is restrained against vertical or radial motion. These brackets stiffen the pipe to minimize any tendency for hydraulic induced vibration by stiffening the system and providing friction damping.

The following factors are analyzed in the design of the sparger and piping:

a. Vortex Shedding
b. Turbulence Excitation
c. Self-excited Vibration Possibilities
d. Parallel Flow Vibration
e. Pulsing Flow
f. Wear as a Result of Above

Centrifugal pumps are used in the core spray system and therefore pulsations such as might be encountered with positive displacement pumps are avoided.

Core Structure, Material and Fabrication Requirements

In addition to the core structure meeting all stress requirements of ASME Section III, for Class A Vessels, certain material fabrication procedures are stipulated and are considered necessary to insure structural integrity of the core structure for normal operation as well as for all routine and accident transients.

All material used in the core structure must meet or exceed the applicable ASME Code requirement. Hardness tests are also required for all materials used to insure the base material is less than Rockwell Hardness 90 B. This requirement minimizes any stress corrosion tendency. Forming operations which would increase hardness are limited. In the event these fabrication requirements are not met, the finished part must be solution annealed.

Fabrication details such as weld joint preparation must be submitted for review and approval by engineering. The following procedures must also be reviewed and approved by engineering:

1. Any heat treatment or stress relief.
2. Welding and repair of welds found defective during inspection.
3. Weld and welder qualifications.
4. Ferrite control in austenitic welds.
5. Liquid penetrant and visual inspection tests of all welds.
6. Cleaning.

All welding procedures and welders must be qualified in accordance with Section IX, ASME Boiler and Pressure Vessel Code or equivalent.

C. Quality Assurance

Materials

The materials specified for the Core Spray System, from the reactor up to and including the second motor operated valve, are Type 304 or 316 stainless steel in accordance with an appropriate ASME material specification as follows:

Pipe - ASTM A312, A376, or A358

Fittings, Flanges and Valves ASTM A183, A336, A351, or A403.

The materials specified beyond the second motor operated valve are carbon steel, in accordance with an appropriate ASTM material specification as follows:

Pipe - ASTM A106 Gr. B or A155 Gr KC70 Class I, Firebox quality.

Fittings, Flanges, and Valves: ASTM A105 Gr. II, A216 Gr WCB or A234 Gr WPB

Fabrication and testing of the Core Spray System Piping and Valves up to and including the second isolation valve will be in accordance with ASME Pressure Vessel Code, Section I; the remaining piping and valves specified for the weld joint design and welding procedure for piping 2 1/2" and larger in size are as follows: 1) Welds are required to be full penetration groove welds. Double welded joints are required to be inspected for soundness prior to welding the back side.

Single-welded joints are required to be made by using (a) U-groove and consumable insert ring with Gas Tungsten Arc Weld process for root and second layer; (b) for carbon steel base metal, U or VEE groove with Gas Metal Arc Weld process and short circuiting transfer (short arc) for root and second layer; or (c) U, VEE, or bevel groove with metal backing which is subsequently removed and the weld inspected for soundness on the back side.

Single welded joints, in stainless steel portion of the system, made by the Gas Tungstem Arc Weld process, without metal backing are required to have a protective gas back purge for the root and second layer.

Inspection and Tests

Additional General Electric inspection and tests beyond those required by the applicable codes are as follows:

Castings	(1)	Radiograph either liquid penetrant or magnetic particle examination.
Forgings	(2)	All forgings - liquid penetrant or magnetic particle examination. Forgings 4 inch thickness-ultrasonic examination.
Welds		All welds - Visual and either liquid penetrant or magnetic particle examination.
	(2)	Pressure containing groove welds - Radiographic examination.
Valve Stems		Ultrasonic and liquid penetrant examination.
Bolting		Pressure containing - Either liquid penetrant or magnetic particle examination.

The system is hydrostatically tested at 1 1/2 times the design pressure except those portions forming an integral part of the containment. These portions will be tested in accordance with the ASME Boiler and Pressure Vessel Code, Section III.

---

(1) Beyond the 2nd motor operated valve (isolation valve) Only valves 4" and larger are required to be radiographed. Liquid penetrant or magnetic particle are not required.

(2) Applicable to components 2 1/2" and larger size.

D. Reactor Pressure Vessel Loadings

Vessel movements of such a magnitude as to jeopardize the essential piping from the drywell to the vessel should be extremely unlikely. This is because the vessel is so strongly joined to its support pedestal.

In order to understand how well the vessel is held in place a study of the forces required to lift the vessel was undertaken. The force necessary to move the pressure vessel upwards as a function of the distance moved is shown in Figure 16. It is seen that the vessel structure is strong enough so that no appreciable movement occurs if the lifting force does not exceed the sum of the vessel weight and the concrete shield weight, or approximately 6.48 million pounds. The feet of the vessel are sufficiently strong that they can lift the entire concrete support structure inside the drywell. When this force is exceeded, vessel movement can occur only by drywell fracturing of the pipes connecting the pressure vessel and the containment. An additional million pounds is necessary for this to occur.

BASIS OF ANALYSIS

The pressure vessel and its various connections to the shield and pedestal are assumed to be axially loaded. The usual formula for axially loaded member is used. This analysis provides an index of the overall strength of the vessel hold-down structure. The 6.5×10^6 lb. of vertical force required to cause relative motion between the vessel and the containment is a factor of 5 higher than the maximum thrust resulting for a full recirculation line rupture. It is concluded, therefore, that even for ruptures in excess of the design break, vessel motion is not possible.

In addition these calculations reveal the large forces required to move the vessel relative to the concrete surrounding it. Because of the large diameter of the piping that penetrate the concrete cylinder around the vessel, it would take 26×10^6 pounds to shear the vessel free. This force is about ten times the weight of the vessel and all the internals. Thus extremely large forces would be needed. Such a force would be possible only if one postulated a 13 foot diameter hole in the bottom of the vessel.

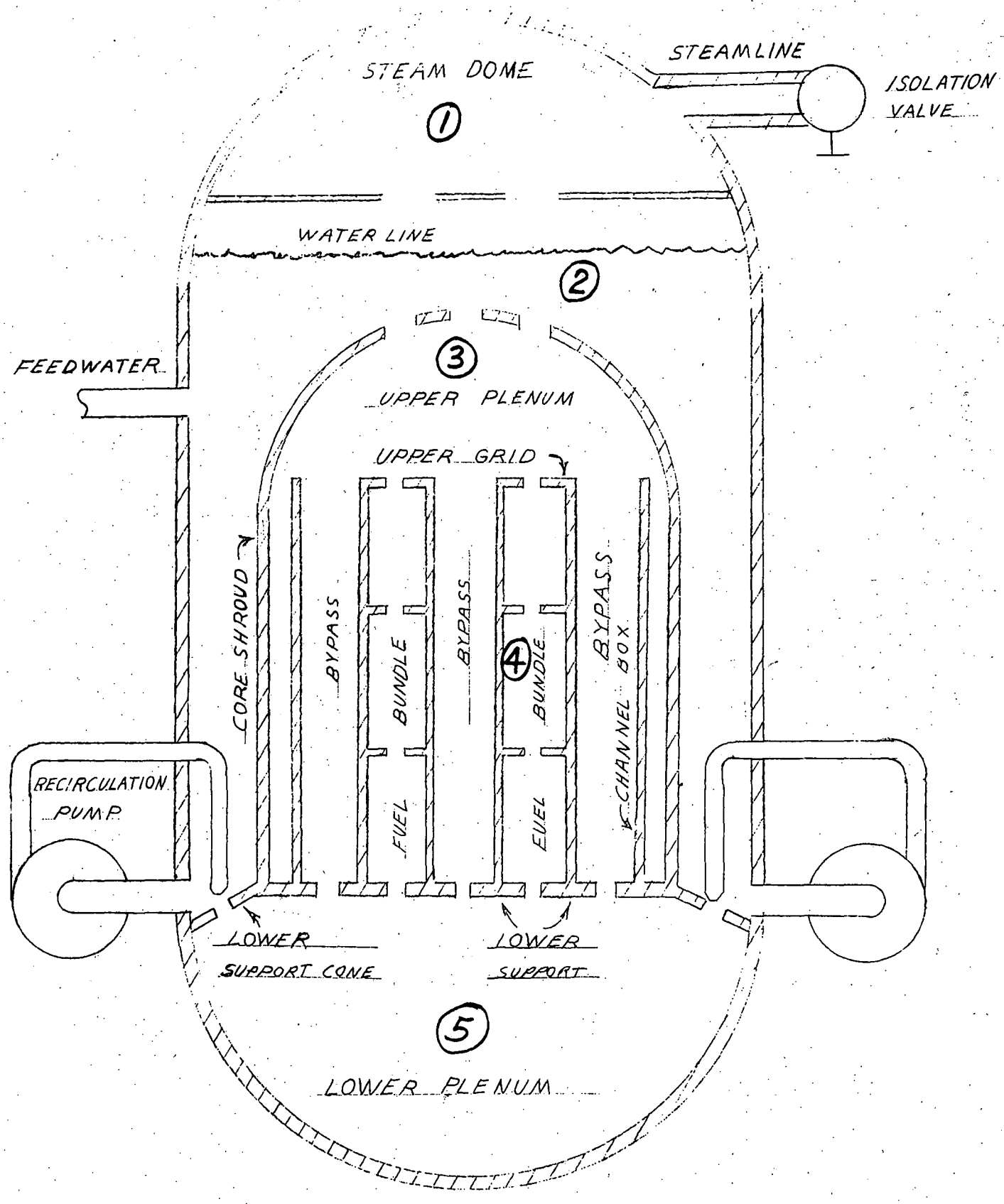

STEAM DOME
1
STEAMLINE
ISOLATION VALVE
WATER LINE
2
3
FEEDWATER
UPPER PLENUM
UPPER GRID
CORE SHROUD
BYPASS
BUNDLE
FUEL
BYPASS
4
BUNDLE
FUEL
BYPASS
CHANNEL BOX
RECIRCULATION PUMP
LOWER SUPPORT CONE
LOWER SUPPORT
5
LOWER PLENUM

Fig II.3.3-1

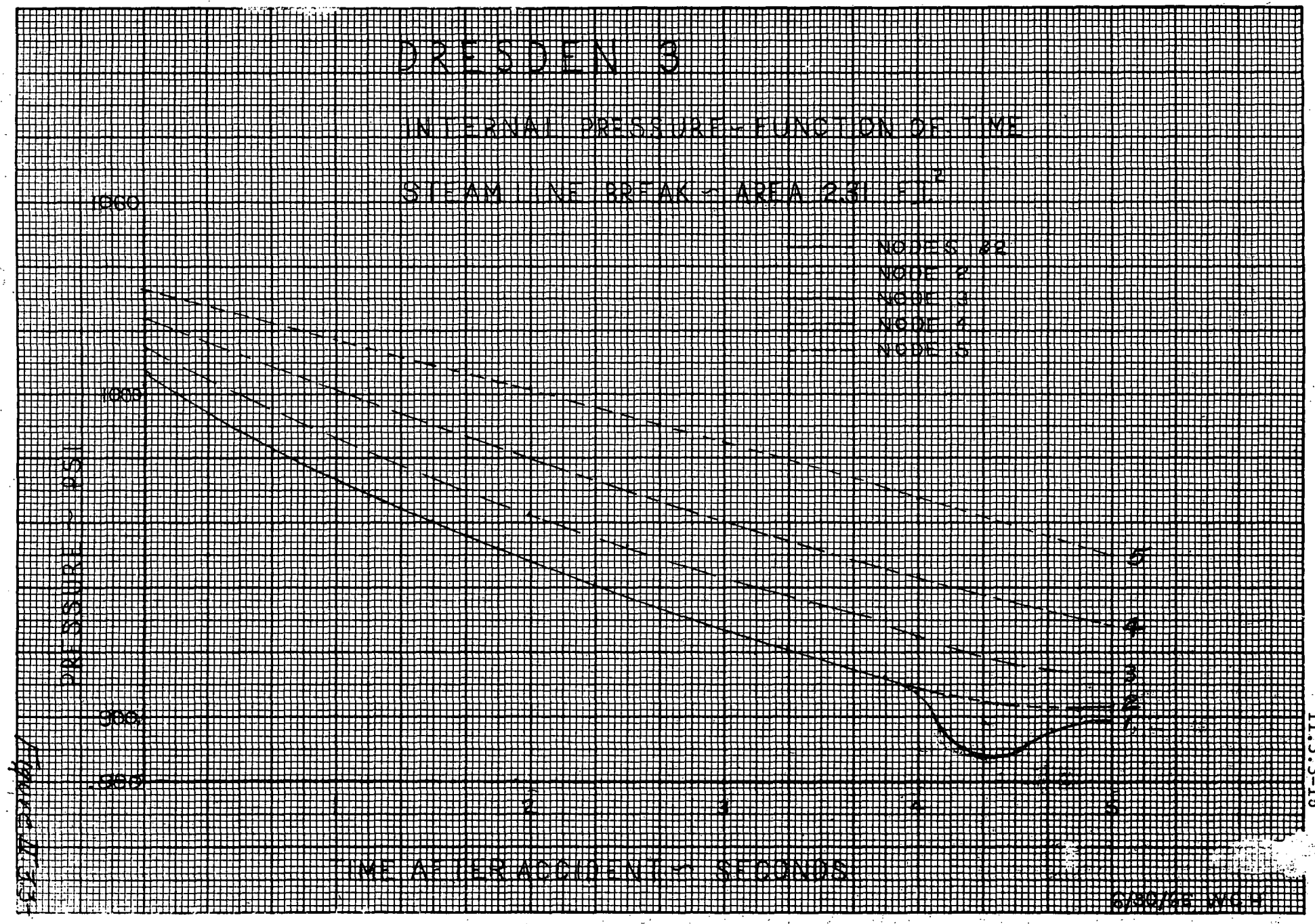


Figure II.3.3-15

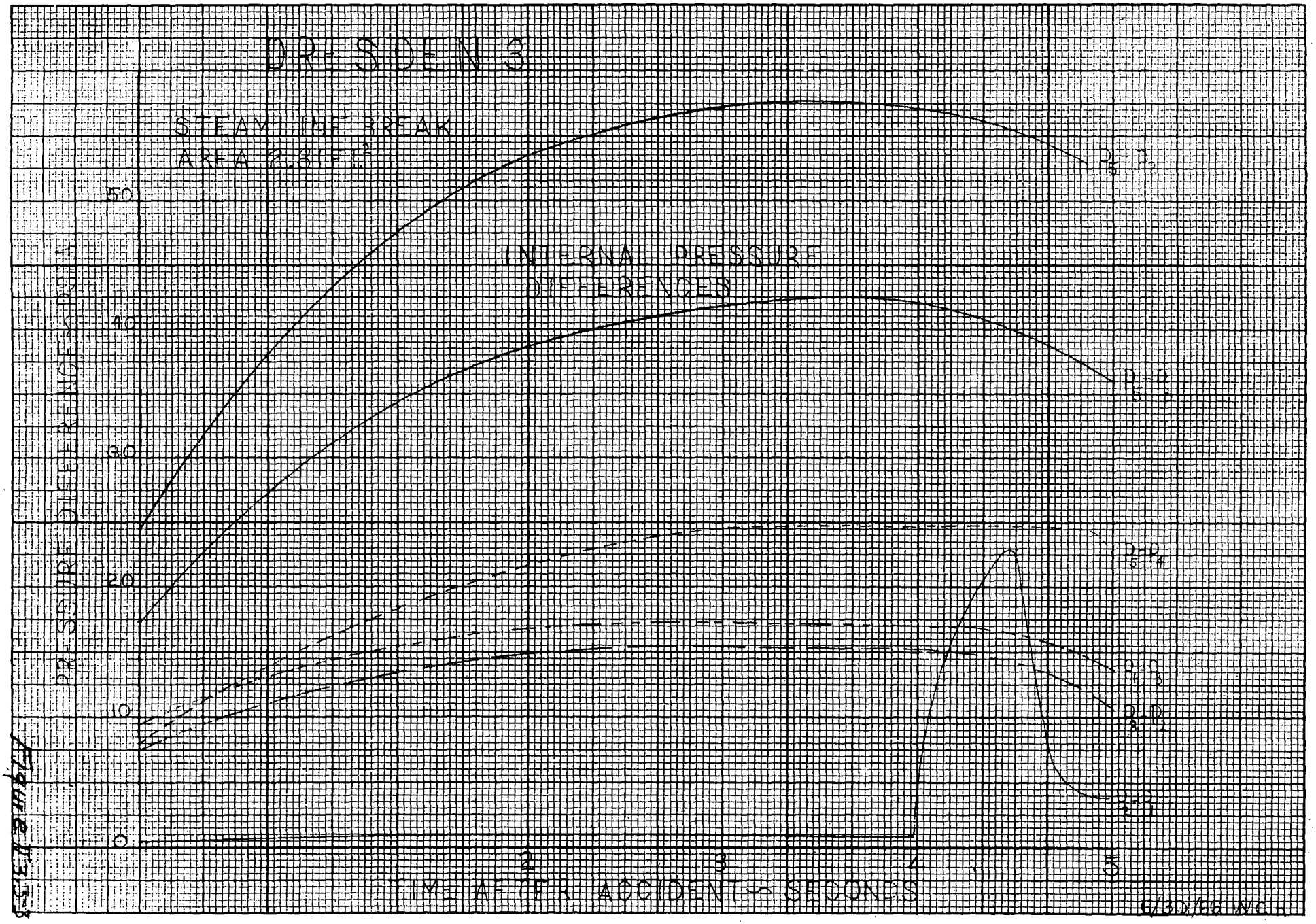
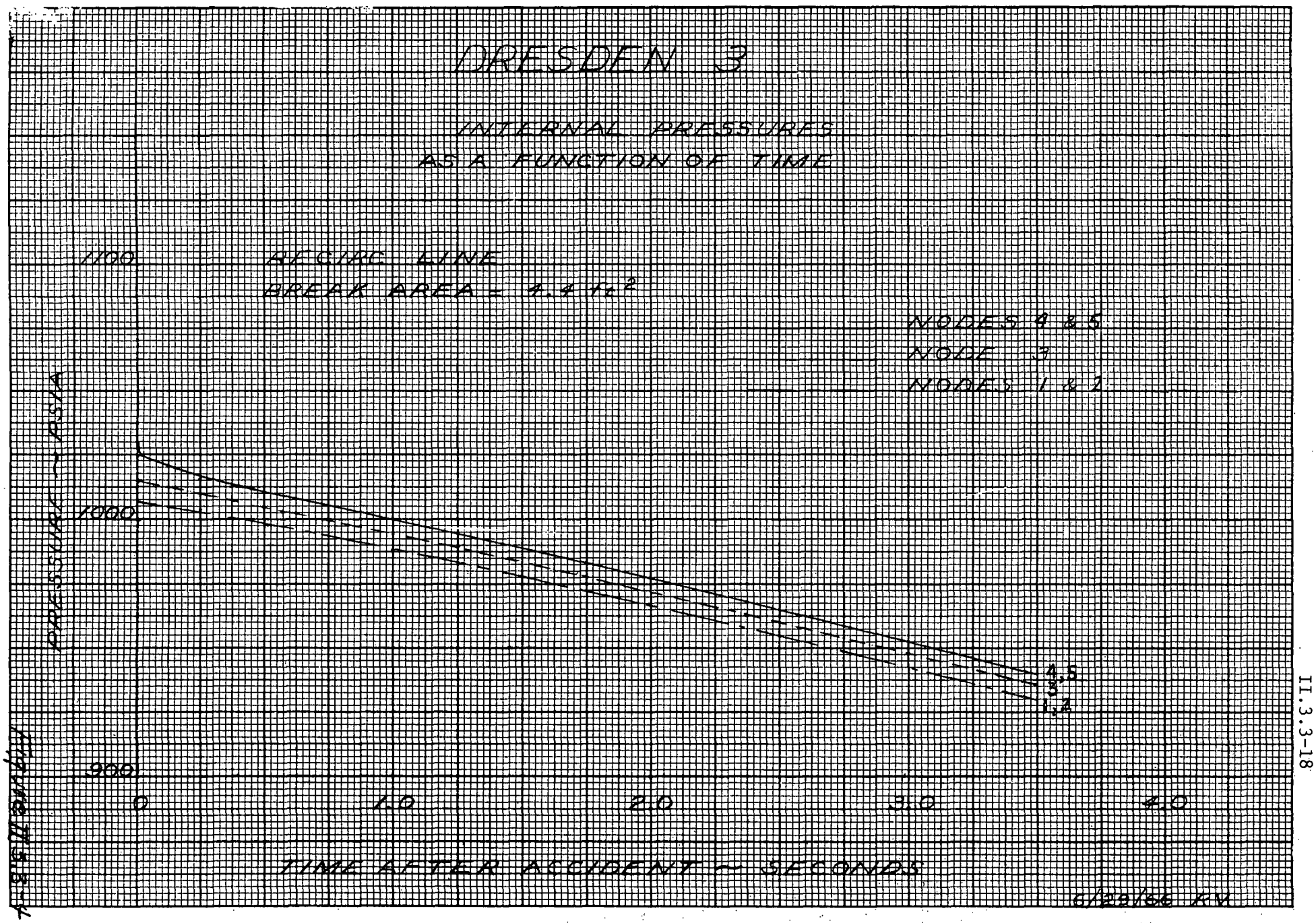


FIGURE II.3.5-8


DRESDEN 3
INTERNAL PRESSURES
AS A FUNCTION OF TIME
RECIRC LINE
BREAK AREA = 4.4 ft²
NODES 4 & 5
NODE 3
NODES 1 & 2
1100
1000
900
PRESSURE ~ PSIA
0
1.0
2.0
3.0
4.0
TIME AFTER ACCIDENT ~ SECONDS
6/29/66 RV

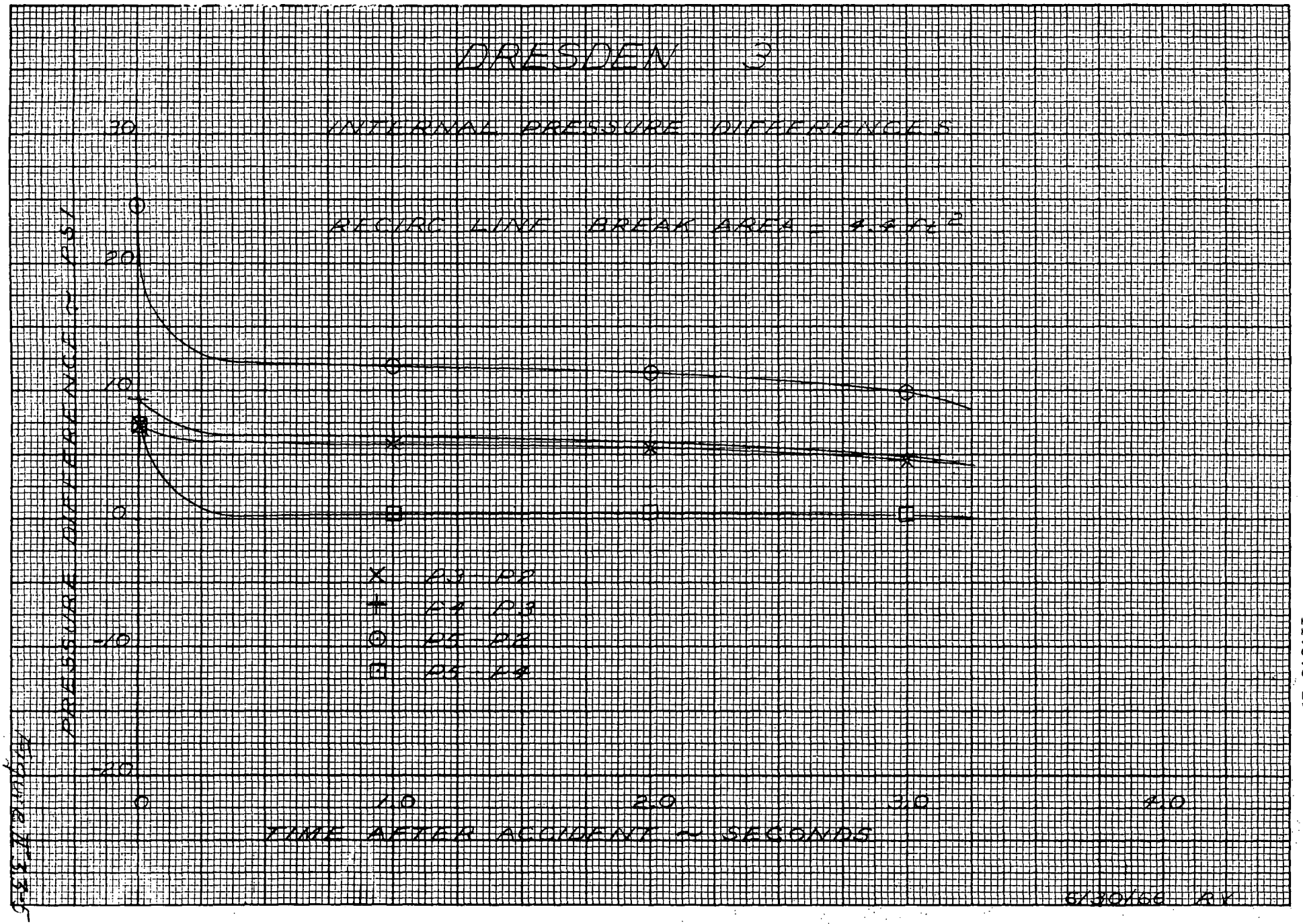


Figure II.3.3-5

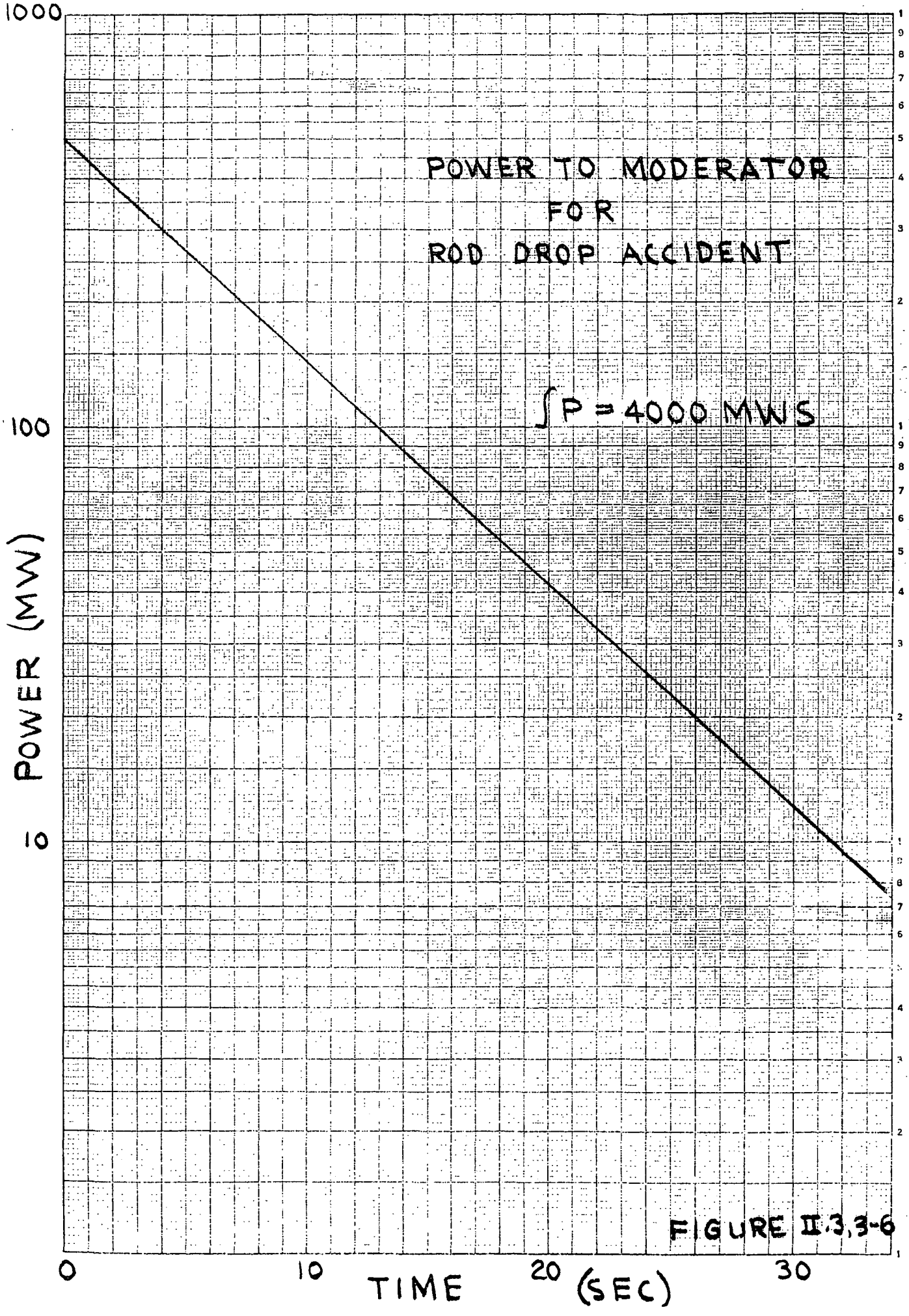


FIGURE II.3.3-6

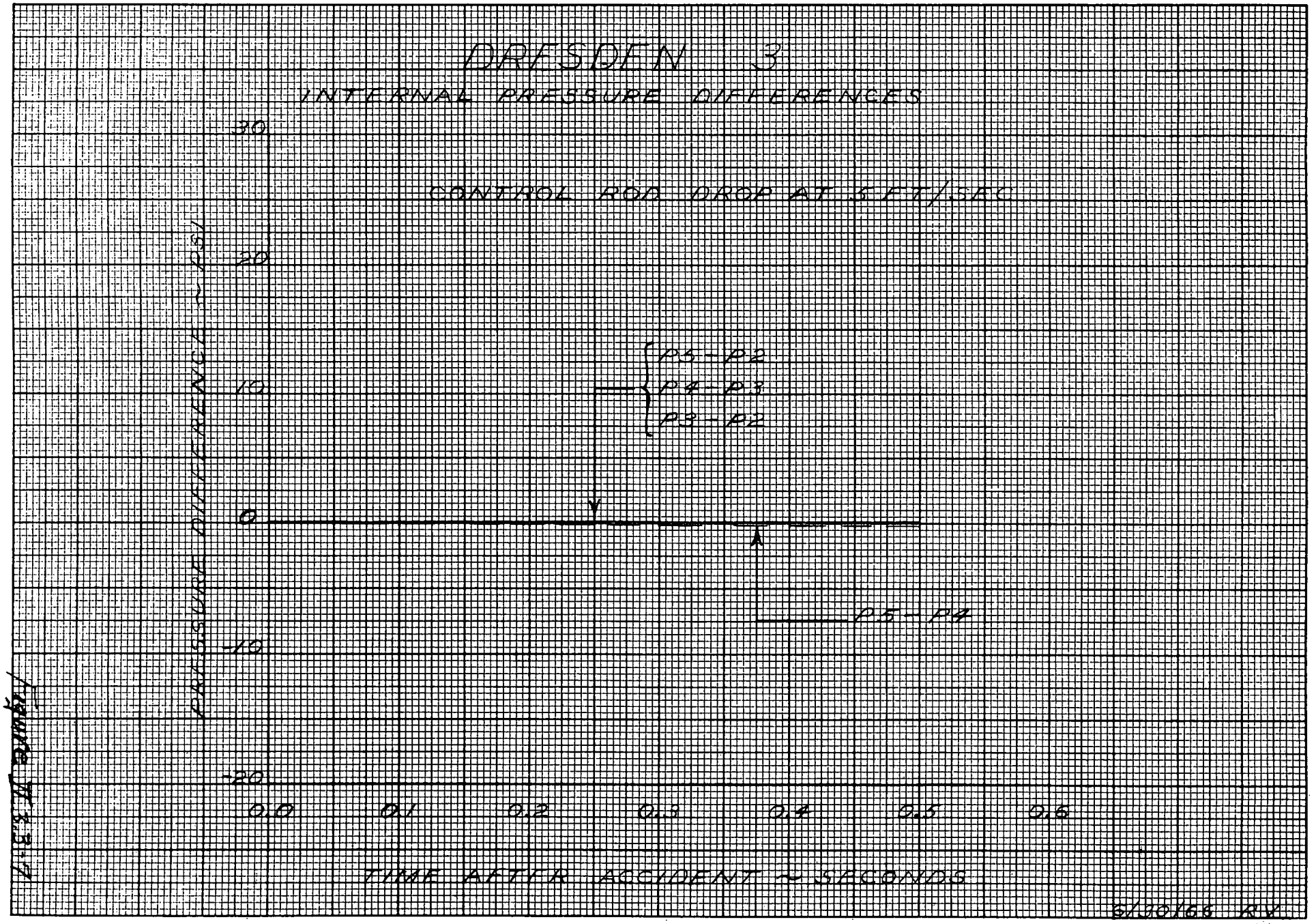


FIGURE II.3.3-7

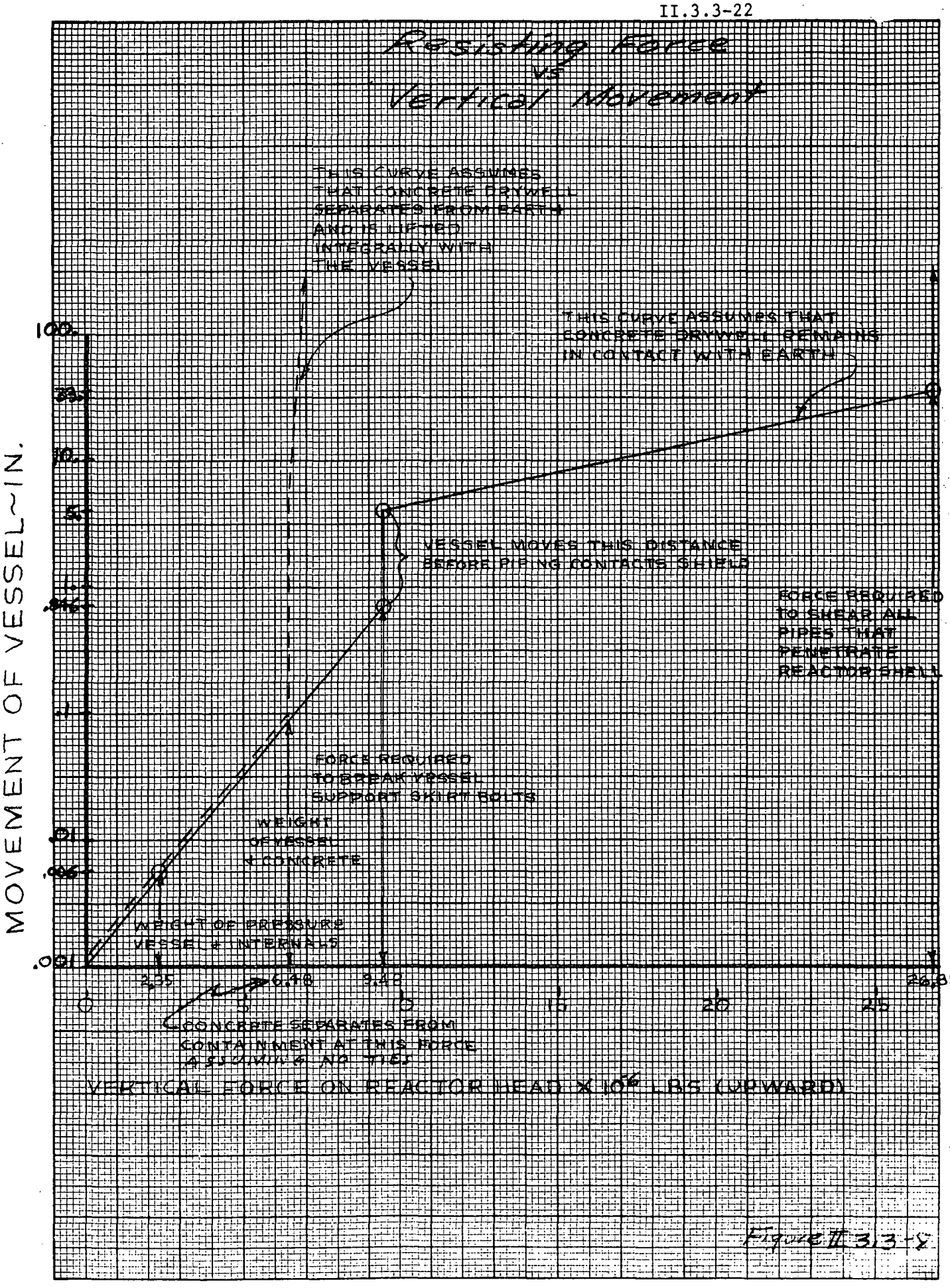


Figure II.3.3-8

II.3.4 Flow Requirements

The required core spray flow to obtain effective cooling was determined from tests on full scale fuel assemblies which simulated initial cladding temperature expected, decay heat, axial power factors, and typical rod to rod peaking. These tests and the cooling phenomena which takes place are summarized in Appendix II-C. The conclusion with regard to the required flow rates is that for all practical purposes, no flow dependency existed over the range of flows tested. Although a lower flow may be adequate, the minimum flow tested was 0.05 gpm per fuel rod. Hence, this was used as the basis for sizing the core spray flow rate; i.e. the minimum flow to any fuel bundle in the core must receive no less than .05 gpm per rod.

The spray flow distribution across the core varies as shown in Appendix II-C. Based on a series of flow distribution tests involving the pertinent parameters such as spray angle, height, etc. and a calculational method which evolved, it was concluded that a distribution factor of 0.4 could be achieved in the Dresden III Core. This means that the minimum flow into any fuel assembly will not be less than 40% of the average flow into all the assemblies.

Thus, the total core spray flow is calculated as:

$$\text{Total Flow} = \frac{0.05 \text{ gpm} \times \#\text{ of Fuel Assemblies} \times \#\text{ of Rods/Assembly}}{0.4}$$

For Dresden Unit 3, this is 4550 gpm assuming that fuel assemblies are present in all the 724 spaces available in the core lattice.

The core spray tests also provided the basic coupling, i.e., a correlation for the effective heat transfer coefficients, such that the measured temperatures could be matched with the core heatup code model used to calculate the core response to core spray over the wide range of conditions in the core.

II.3.5. Effectiveness of Core Spray

That the core spray is an effective cooling mechanism was demonstrated in the prototype tests covered in Appendix II-C, Section 2. These tests eliminated various concerns and showed that:

a. Water can enter a hot fuel bundle and is not forced back by steam formation.

b. Although the rods are not wetted, cooling is effective through strong radiation heat losses to the water, steam and channel walls, supplemented by some convective cooling. Thus, it is believed that such parameters as rod spacing and flow geometry are not important to effective cooling, and that the rods could even touch locally without destroying the cooling mode.

c. The thermal model used in the TACT V core heatup code could predict with reasonable accuracy the temperatures measured in the core spray tests.

d. A correlation exists for the effective heat transfer coefficient as a function of initial starting temperature and power thus providing confidence that various off-design evaluations can be evaluated.

The effectiveness of the core spray system for a spectrum of breaks is evaluated in Section II.3.

II.3.6 Timing Sequence

II.3.6.1 Initiation

The system must (1) initiate operation automatically when required and (2) be in operation within the required time.

Reliability of the reactor low-level sensing signal is assured by using four independent level sensing switches connected in one out of two twice array. These are float switches of a type which have been thoroughly evaluated for use in the reactor protection system.

The sequence of automatic operation and the time delays involved are described in Section II.2 and shown graphically in Figure II.3.6-1. For the design break accident with availability of AC power from either external plant sources or the diesel generator (which is started automatically on low reactor water level); this set of conditions together with successful start of the second pump would take 28 seconds for design break. Even under the adverse condition of loss of AC power simultaneously with receipt of low reactor water level signal and assumed failure to start of one of the core spray pumps, the total time to achieve full rate core flow is 35 seconds.

II.3.6.2 Operation

The core spray pumps can be operated from power generated by the standby diesel generator or external power sources. In the case of a small break, water level would be restored through operation of the system, and on receipt of a high level signal and alarm the operator would shut off the pumps. Steam generated in the system would have to be relieved through the leak or by operation of the relief valves which are operable from the control

room and adjustable in set point down to approximately 150 psig reactor pressure. For larger breaks where cooling would be by core spray, the system could be operated continuously as long as required. The diesel generator feed system provides fuel storage for a minimum of 100 hrs. continuous operation during which time additional fuel can be brought into the plant.

The diesel generator will be sized to start and accelerate the following loads (sequentially if necessary): (Sizes shown are preliminary)

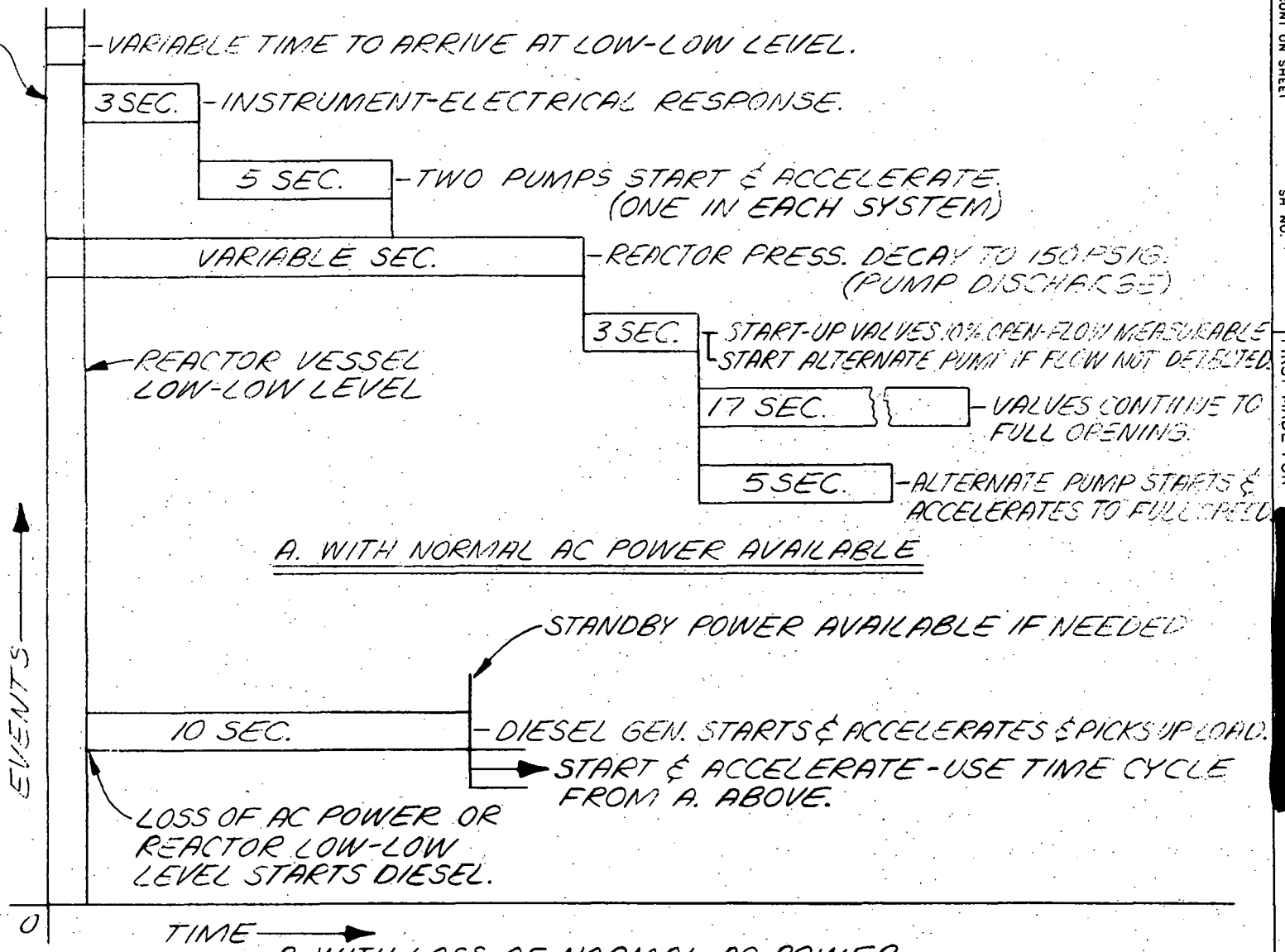
Core Spray	1200 hp
Containment Spray	700
Service Water	900
Standby Gas Treatment	75
Emergency AC Lighting	100
	2975 hp

In the event of a small break size, level could be maintained by the core spray pumps and the reactor vessel vented as required. Removal of reactor decay heat could be transferred to the shutdown cooling system and servicing functions carried on in a manner similar to a normal refueling to prepare the system for the repair operation.

In the event of a large break for which level could not be practically maintained, the reactor would be depressurized and the primary containment filled with water to prepare for the repair operation. The total volume in the containment is approximately 275,000 ft^3. The flow of one core spray pump (4500 gpm) could fill this volume in about 8 hrs. Connections are provided from the condensate storage tank which could be used for this

purpose. There are normally about 250,000 gallons (32,000 ft^3) in this tank; it in turn can be replenished from the service water pumps or the fire water system.

After the containment has been filled to approximately the flange line of the reactor vessel, the head can be removed and the fuel removed from the reactor and transferred into the fuel storage pool. This would permit decontamination measures to be carried out, inspection and necessary repairs.



B. WITH LOSS OF NORMAL AC POWER.

CORE SPRAY RESPONSE TIME AFTER LOSS OF COOLANT

FIGURE II.3.6-1

GENERAL ELECTRIC

II.3.3-4

TITLE CORE SPRAY (RESPONSE TIME)

FIRST MADE FOR [redacted]

REV NO. 0

SK56121-928 CONT ON SHEET SH NO.

SK56121-928 CONT ON SHEET SH NO.

REVISIONS 0

MADE BY G. G. Bu. JULY 26, 1966 ISSUED

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II.3.7 Core Response

The response of the core is calculated in two phases, ie, the blowdown phase in which the mass is expelled and the core heatup phase during which the core heats up and eventually cools due to the core cooling systems.

The blowdown of the vessel is based on the model of F. J. Moody described in ASME Paper 64 HT-35 "Maximum Flow Rate of a Single Component, Two Phase Mixture". For the large breaks, in excess of about 1/2 square foot either critical liquid flow or two phase critical flow give nearly the same results since the blowdown is rapid in either case.

For the sudden breaks the flow, for breaks below the water level, is liquid critical flow in which the vessel depressurizes relatively slow. A distinct level exists, falling with time and uncovering the core while the pressure remains relatively high. The break is then uncovered as the level drops below it and depressurization becomes relatively rapid since steam blowdown then occurs while the remaining liquid mass is very small. In the model, the Core Spray is introduced as a function of vessel pressure. The overall blowdown model is described in Appendix III-A.

The thermal performance of the core from the time of the break is evaluated using the TACT V Core heat up code described in Appendix II-E.

The reactor core is subdivided into radial and axial positions. The fuel bundles are further divided into zones of fuel rods. Thus, the reactor core fuel subdivision allows the analysis to be conducted with a refined specification of power distribution throughout the reactor core, radially and axially, as well as throughout the fuel assemblies and fuel rods for a total of 525 Nodes. With this same degree of subdivision the temperature distributions throughout the reactor core can be quite precisely determined throughout the course of the temperature transient. Although radiation heat transfer from fuel rod to fuel rod and to the channel box is considered by the program, there is no heat loss from the overall reactor core itself allowed except for any energy carried away by flow through the core. For example, the core spray system can remove energy, water and hydrogen associated with a metal water reaction can also remove energy. Thus,

decay heat and metal-water reaction heat during the core heatup process before core spray becomes effective are retained within the core in the form of sensible heat and latent heat of fusion. Fuel channel box material is analyzed separately from the fuel cladding and fuel pellet material. Because of the emphasis placed on the existence of any metal water reaction, the digital computer program also involves a continuous calculation of the extent and current rate of the metal water reaction for all metal surfaces within the reactor core on the above described subdivision basis. The metal-water reaction is defined in the computer program by the expression of Baker, (See Ref. 2), which defines the rate of reaction as a function of both local metal temperature and the current extent of the reaction. The model assumes an unlimited steam supply for metal water reaction purposes.

The removal of core stored heat during the blowdown phase is important for the large breaks in which the core is uncovered within a few fuel rod time constants. This was determined experimentally as described in Appendix II-D from blowdown tests on heated fuel rods. The heat transfer coefficients during blowdown are programmed as a function of time in the core heatup code. After blowdown is complete, the coefficient is assumed to be zero and the core remains insulated until core spray becomes fully effective ie, the experimentally determined core spray heat rransfer coefficients and heat sink is applied as a boundary condition to the core heat up code.

For the smaller breaks, in which the core is not uncovered within several fuel time constants (~30 seconds) all the stored heat will be removed. This is because the depressurization rates are sufficiently slow that the recirculation pumps continue to run and are virtually unaffected until the core stored heat is removed. Hence, the blowdown heat transfer data applies only for large breaks (rapid depressurization) in which the recirculation pumps cavitate within a few seconds or cannot be relied upon to provide adequate core flow.

For the smaller breaks core cooling is defined as adequate until the core is half uncovered. This is justified in Appendix II-C, Section 3, and is based on experimental data.

A parameter study of the Dresden Unit 3 core resulted in a curve, Figure II.3.7-1 showing the allowable time the core could remain uncovered, after being adequately cooled for various times, before local clad melting occurs. Thus, if no clad melting is the criteria, the allowable time for

uncovery can be found easily as a function of cooling time, ie, break size.

Another curve, Figure II.3.7-2 shows the amount of metal water reaction which takes place as a function of core uncovery time. Since at the very most, less than 0.5% metal water reaction would occur during the time core spray is cooling the core, and since about 1% maximum metal water reaction can be tolerated within the flammability limits, it follows that the maximum allowable metal water reaction for the period of time before core spray effectiveness is about 0.5%. This, then limits the time that the core can be allowed to remain uncovered as a function of core cooling time, the latter being a function of the break size. For the small break sizes of interest for the High Pressure Coolant Injection System and the lower limits of the core spray system, a conservative core uncovery time of 500 to 600 seconds is allowed for this evaluation. This criteria allows a very consistent basis of evaluation to exist, even though the <u>specified design basis</u> for the core spray system is the prevention of any fuel melting. The use of the more conservative basis of metal water reaction extent therefore insures satisfaction of the no fuel melt design basis.

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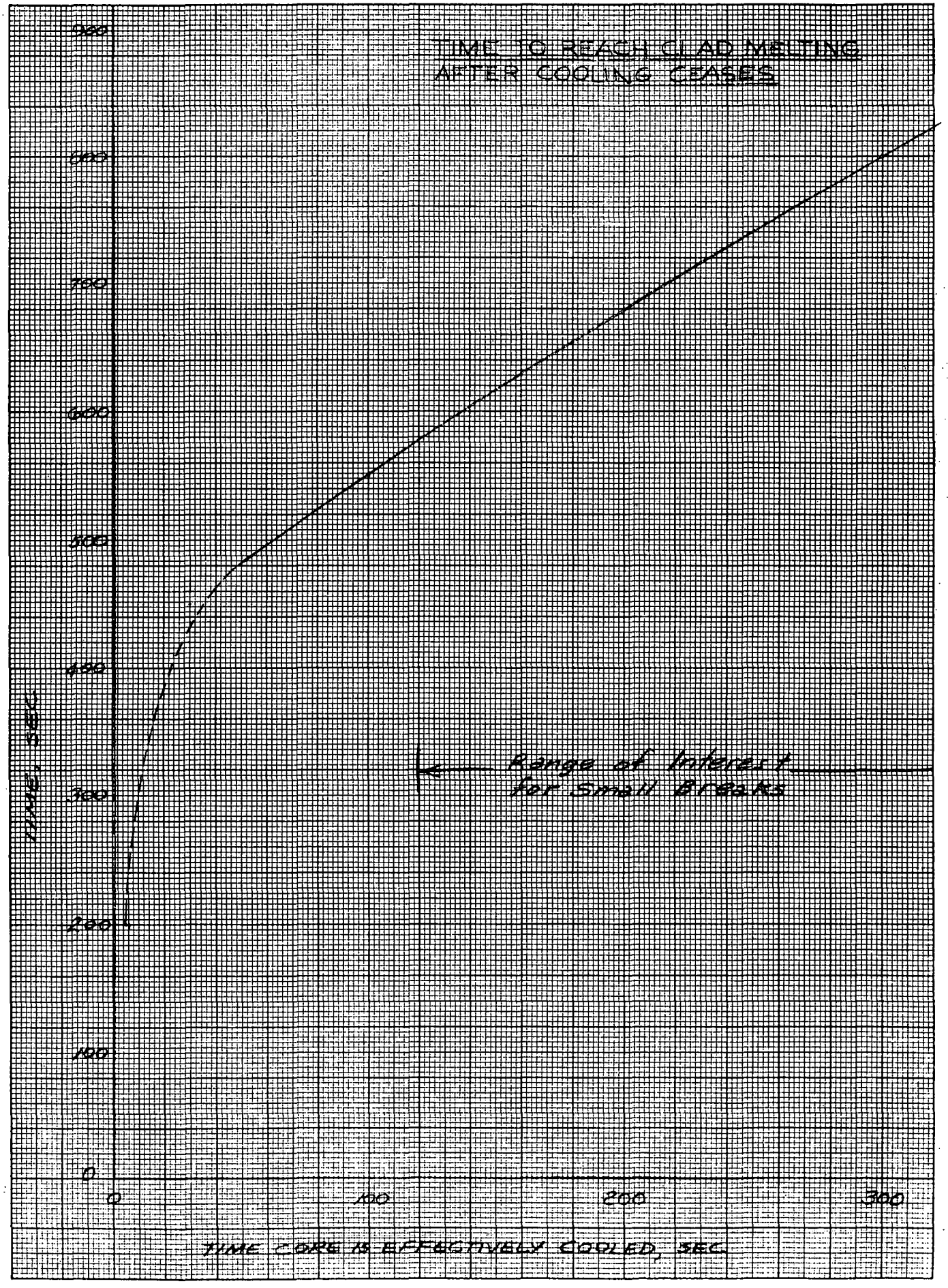


Figure II.3.7-1

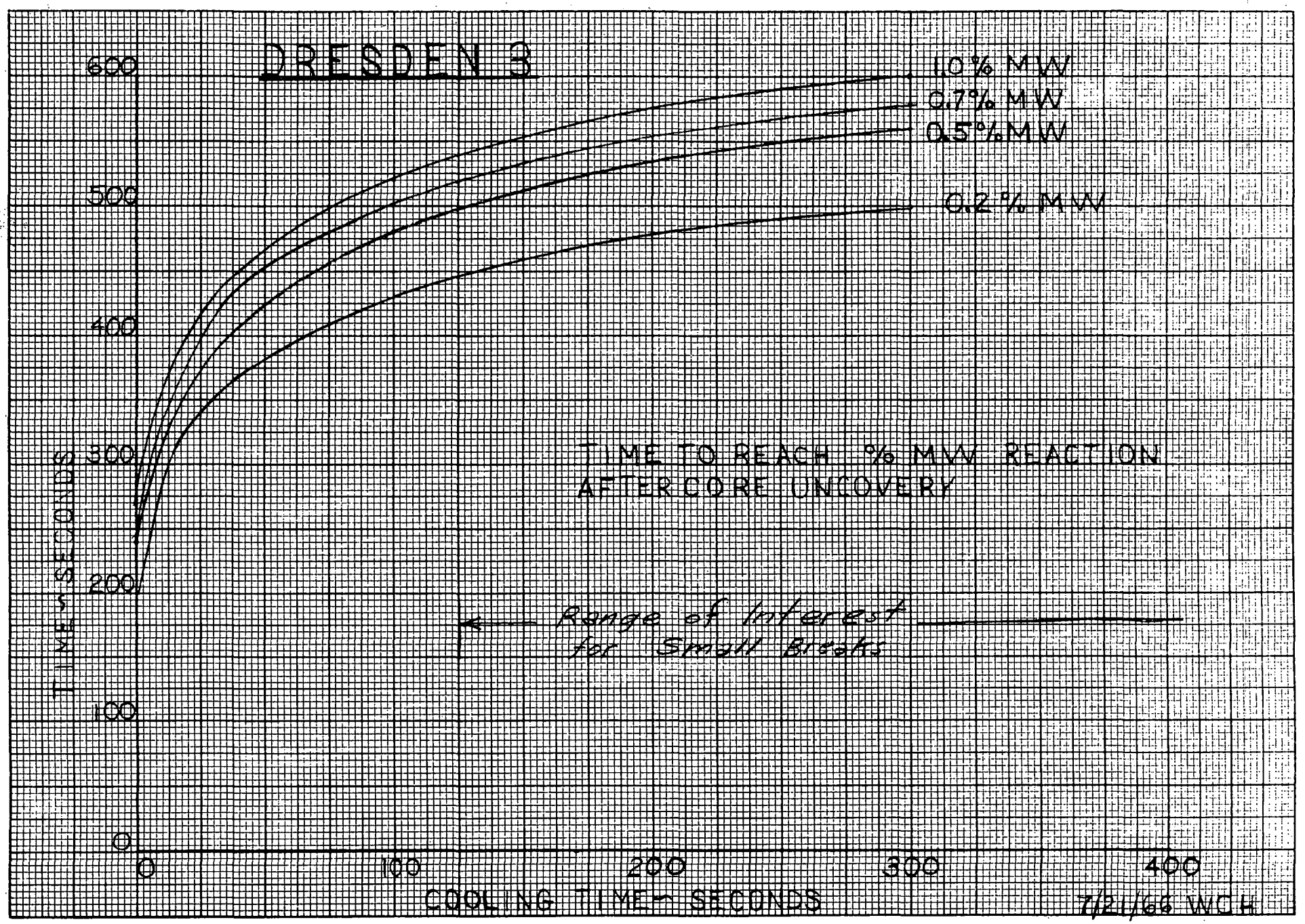


Figure II.3.7-2

II. 3.8 Core Thermal Response - Design Break Accident

The design basis accident is the sudden rupture of one of the main recirculation lines. The reactor is operating at the design thermal output and the recirculation loop is assumed to be instantly severed in a circumferential break. In the reactor vessel outlet leg of the recirculation loop, critical flow occurs at the break. Critical flow also occurs at the 10 jet pump injection nozzles, the minimum area in the path to the break. The equalizer line valve is normally closed during two pump operation. However, for the purpose of this analysis it was assumed full open with critical flow occurring in the pipe with zero losses. Thus the total break area at which critical flow is occurring was assumed to be the sum of the suction nozzle area, the 10 jet pump injection nozzle area, and the equalizing line area or 5.5 ft^2. Actually the true equivalent area using a rational analysis with friction included has been calculated to be 4.2 ft^2 even with the equalizing line valve open and somewhat less with the valve closed.

Immediately following the break, the large increase in core void fraction due to depressurization decreases reactor power. Scram will also be initiated in less than a second from high drywell pressure. Relative to the total blowdown time, the pressure regulator quickly closes the turbine admission valves in an effort to maintain pressure. Hence, steam flow will be a contributor to depressurization only during the first few seconds. Complete depressurization takes about 24 seconds.

It is not necessary to track the level during the blowdown since the effective

core cooling time during such rapid blowdowns can be obtained from the test data in Appendix II. The heat transfer coefficient vs. time used for each zone in the TACT V heatup code is shown in Figure II.3.8-1. Core spray as discussed in Section II.3.6 is fully effective in 28 seconds. A temperature map of the core showing the volume percent of cladding above the indicated temperatures is shown in Figure II.3.8-2. A plot of the percent metal-water reaction is also shown in the same figure.

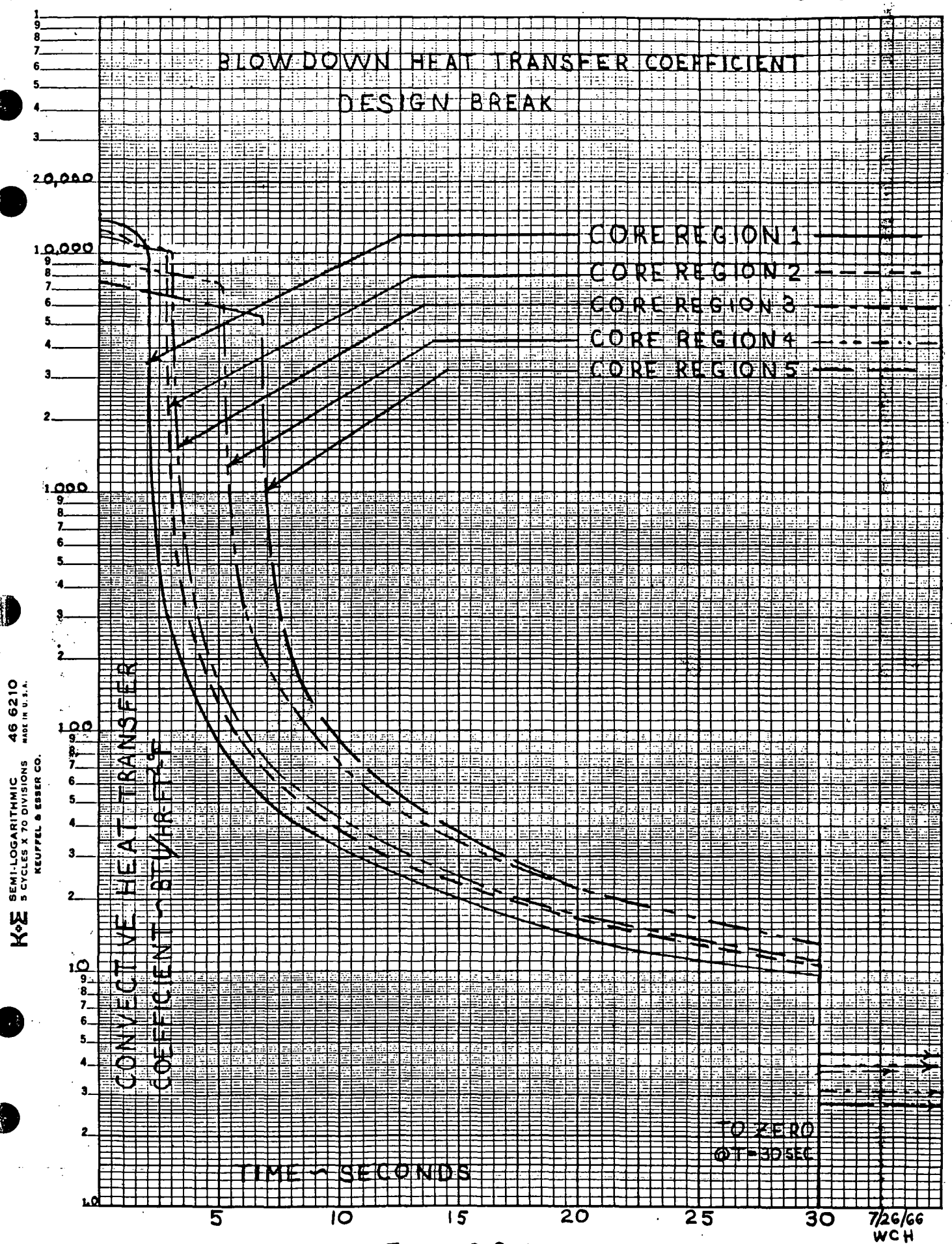
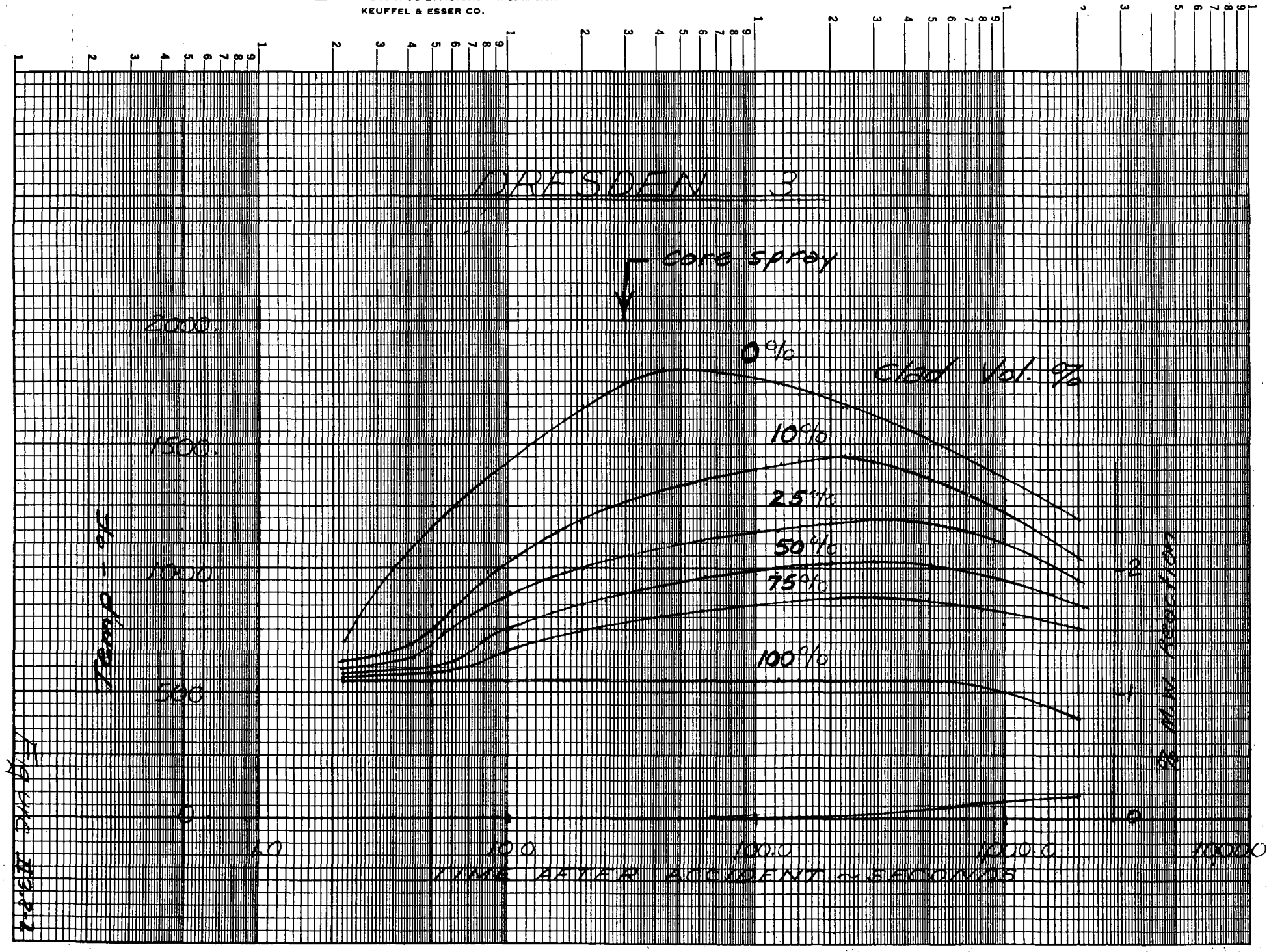

BLOWDOWN HEAT TRANSFER COEFFICIENT
DESIGN BREAK
CORE REGION 1
CORE REGION 2
CORE REGION 3
CORE REGION 4
CORE REGION 5
CONVECTIVE HEAT TRANSFER COEFFICIENT ~ BTU/HR-FT²-°F
TIME ~ SECONDS
TO ZERO @ T = 30 SEC
20,000
10,000
1,000
100
10
1.0
5
10
15
20
25
30
7/26/66
WCH
K+E SEMI-LOGARITHMIC 46 6210
5 CYCLES X 70 DIVISIONS MADE IN U.S.A.
KEUFFEL & ESSER CO.

Fig II 3.8-1



II.3.9 CORE THERMAL RESPONSE - BREAK SPECTRUM ANALYSIS WITH FEEDWATER AVAILABLE - RCIC NOT AVAILABLE

The concern over breaks smaller than the largest design break exists because if the break is sufficiently small, and if no operator action is assumed, liquid can leave through leaks below the core without an accompanying pressure decrease. The core could then remain uncovered and uncooled until all the liquid mass has left the vessel and steam blowdown reduces the pressure allowing core spray to activate. Figure II.3.7-1 from Section II.3.7 indicates the effect of time that the core is uncovered.

The analysis of breaks falls into 3 categories: large breaks, intermediate, and small. The large breaks are those large enough that the feedwater is not important because of the rapidity of depressurization. These breaks are characterized by the fact that depressurization is rapid enough that core spray is activated before the core overheats. Such breaks range from the main recirculation line break to breaks equal roughly to 50% of the feedwater flow capacity or about 0.15 ft^2.

The second category or intermediate breaks are those of a size such that the feedwater storage in the hotwell can supply the leak for a period of time. These breaks range from about 0.15 ft^2 down to within 2-3% of the feedwater capacity (.01 ft^2). These breaks are characterized by the fact that they are large enough to scram the reactor on high drywell pressure and yet are small enough that the operator has time to take manual remedial action.

The third category are those below 2-3% of the feedwater capacity. They are of such a size that the leak can be continuously supplied by the condensate transfer pump and the leakage removed by the drywell sump pump. These leaks are characterized by the fact that continued plant operation is possible from a safeguards viewpoint even in their presence.

a. Large Breaks

Analyses have been performed for various break sizes down to those in the range of the feedwater flow rate (0.15 ft^2 break area). These analyses are based on the same model and assumptions used for the design break. Whether one assumes feedwater availability or not does not appreciably affect the results for these sized breaks. The core temperature rise and metal-water reaction as limited by core spray is less severe than for the recirculation line break. This conclusion covers most of the break area spectrum down to 0.15 ft^2.

b. Intermediate Breaks

However, the results of this analysis for breaks approaching the feedwater flow becomes increasingly too pessimistic as the break size becomes smaller. This is because feedwater will continue to supply the break until the hotwell storage is exhausted. There is an effective 3.8 full flow minutes of feedwater storage in the hotwell which can supply the leakage for a significant period after the break depending on its size as shown in Table II.3.9-1.

TABLE II.3.9-1 INTERMEDIATE BREAKS

Break Area ft^2	% of F.W. Flow	Time to Reach High D.W. Pressure	Time to Use Hotwell Storage	Additional Time to Uncover Core	Additional Tot. Time Allowable Before CS * Must Come On	Tot. Time Available for Oper. Action **
.01	3%	55 sec.	Indefinitely			Indefinitely
.03	10%	30 sec.	40 min.	30 min.	10 min.	80 min.
.06	20%	15 sec.	20 min.	20 min.	8 min.	48 min.
.15	50%	5 sec.	8 min.	6 min.	5 min.	no action required

*Such that core damage in terms of metal-water reaction, or maximum temperatures reached do not exceed those experienced for recirculation line break. See Figure II.3.7-1.

** Includes depressurization time, approximately 5 to 10 minutes.

Then, once it is exhausted there is the additional period of time to lower the level below the top of the core. This latter time ranges from 20 minutes for a break equal to 10% of feedwater flow to 6 minutes for a break equal to 50% of the feedwater capacity.

Since the reactor has scrammed, the feedwater temperature drops to that of the condensate. The level controller would maintain the level to match the leak rate. The 3.8 full flow minutes of water addition represents roughly a complete volume change of the vessel contents. Thus it is an effective depressurizing agent and accounts for the long time it takes to uncover the core.

Reactor scram is assured automatically for breaks down to .01 ft^2 (3% of feedwater flow) which cause a trip from high drywell pressure in reasonable time as shown in Figure II.3.9-1. Condensation on the metal and concrete was considered in evaluating the actual trip times. For breaks in this category, after the reactor scrams and containment isolates, the operator then has a significant period of time in which to assess the situation and take manual remedial action, such as depressurizing the vessel. An examination of Table II reveals that the times available before core spray must be actuated are significant. The operator could make provision for transferring more water to the hotwell thus assuring continued core covery and further depressurization. Also he could activate the emergency condenser and thus accelerate the depressurization. Lifting all of the relief valves could depressurize the vessel in 10 minutes assuming steam flow, or in 5 minutes if two phase blowdown occurs through the relief valves.

When the feedwater storage is exhausted, the vessel pressure has already dropped to low values. Thus additional cold feedwater, or activation of the emergency condenser could easily depressurize the system to under 150 psig even before the core uncovers.

c. Small Breaks

These breaks can be tolerated indefinitely since the condensate transfer pumps can make up the leakage. Even for breaks in the order even a few percent of the feedwater capacity the operator has many indications of a leak in the system. The following off-normal indications will occur, which even an average operator should note quickly:

1) A significant increase occurs in drywell temperature.

2) The sump pump runs continuously or the sump level increases to the alarm point.

3) The feedwater flow is above normal for the specific operating power level.

4) The feedwater pump motor power input will be above normal.

5) A gradual increase in containment pressure will occur.

6) A gradual loss of condensate inventory occurs.

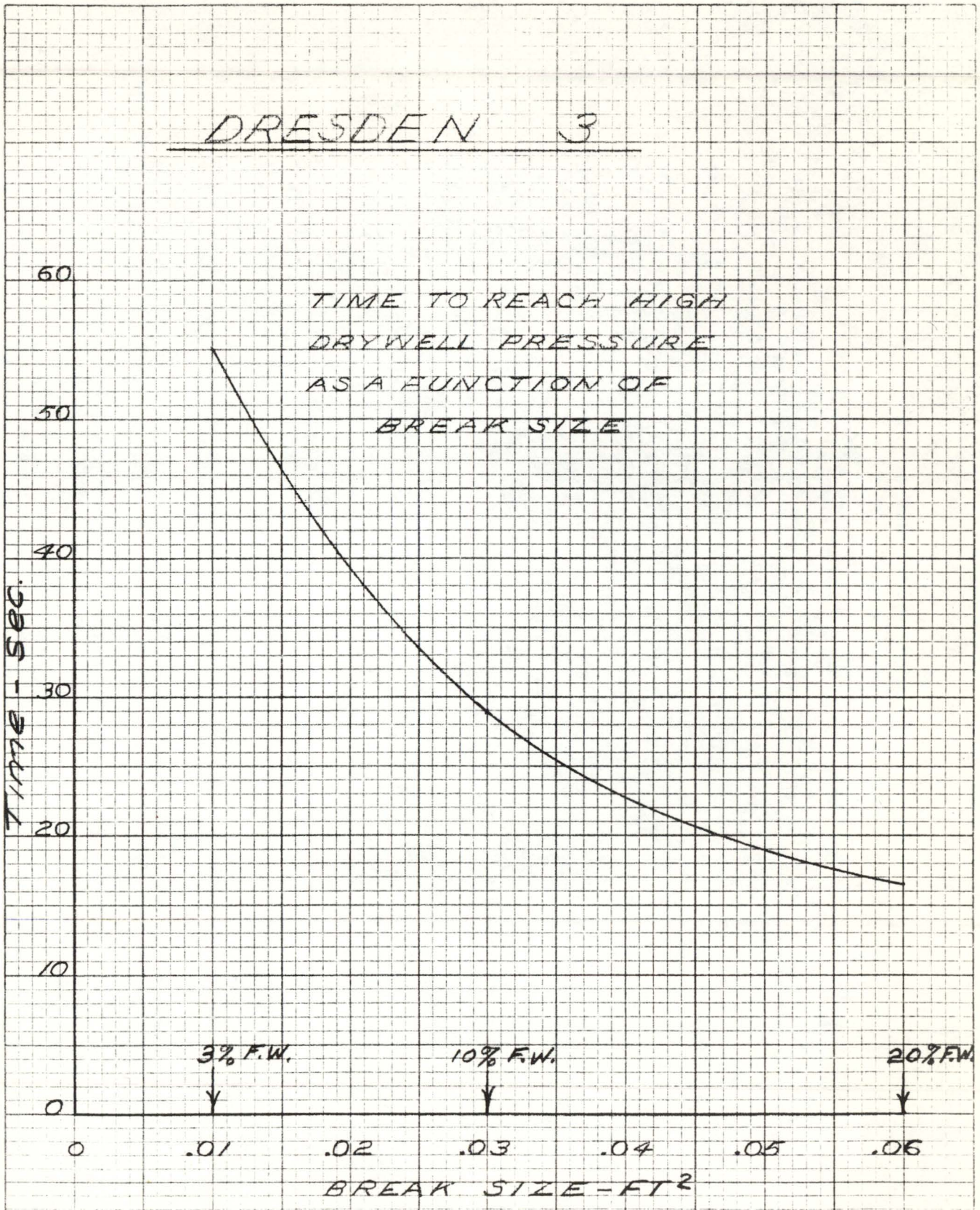


Figure II.3.9-1

II. 3.10 CORE THERMAL RESPONSE - BREAK SPECTRUM ANALYSIS NO FEEDWATER - NO RCIC

Introduction

In order to understand how core spray protects the core over a spectrum of breaks, note the significant parameters in Figure II 3.10-1 for small line break of 0.15 ft^2 below the water level for a condition of no feedwater available. The core would be uncovered in about 150 seconds. In about 450 seconds the vessel would have essentially lost all mass such that steam blowdown would occur and the vessel would depressurize rapidly. The core spray would begin to inject flow when the vessel pressure dropped below about 300 psi, but rated core spray flow would not occur until the vessel pressure was about 70 psia. Since the core spray flow would be less than its rated value until that time the core is conservatively considered uncooled for 500 seconds. Since the core was cooled for 150 seconds after scram by the blowdown action, Figure II 3.7-1 indicates that no clad melting would have occurred. The metal water reaction during the uncovery would only be 0.4%. Even allowing another 0.5% metal water reaction for the period prior to eventual core flooding by the core spray, the total metal water reaction would be less than the 1% flammability limit. Thus this break size is adequately protected by core spray without assistance from either feedwater or the High Pressure Coolant Injection System.

The pressure in the vessel after core spray activation will rise slightly due to the liberation of core decay heat by the spray action. This quasi-equilibrium pressure is determined by the equilibrium established between the energy associated with the mass leaving the break and the required pressure differential for such a mass flow rate. For an 0.15 ft^2 break the resulting pressure level with the vessel is less than 70 psi. Thus the core spray flow would not be reduced below the rated core spray flow value.

There are cases in which, due to operator action, feedwater availability, or other combinations of circumstances, the core spray could be actuated for breaks even less than 0.15 ft^2. The core spray would then come on and be fully effective but if it were left on its own, the pressure could exceed 70 psia due to an inability to emit sufficient steam from the vessel. This action would cut off core spray. However, for each break size less than the 0.15 limit there exists a time in which the heat input is low enough that the 70 psia vessel pressure is not exceeded. Figure IV-1 of Section IV graphically presents typical values of time and associated break sizes for which the vessel would not exceed 70 psia in emitting sufficient steam flow to have a pressure equilibrium.

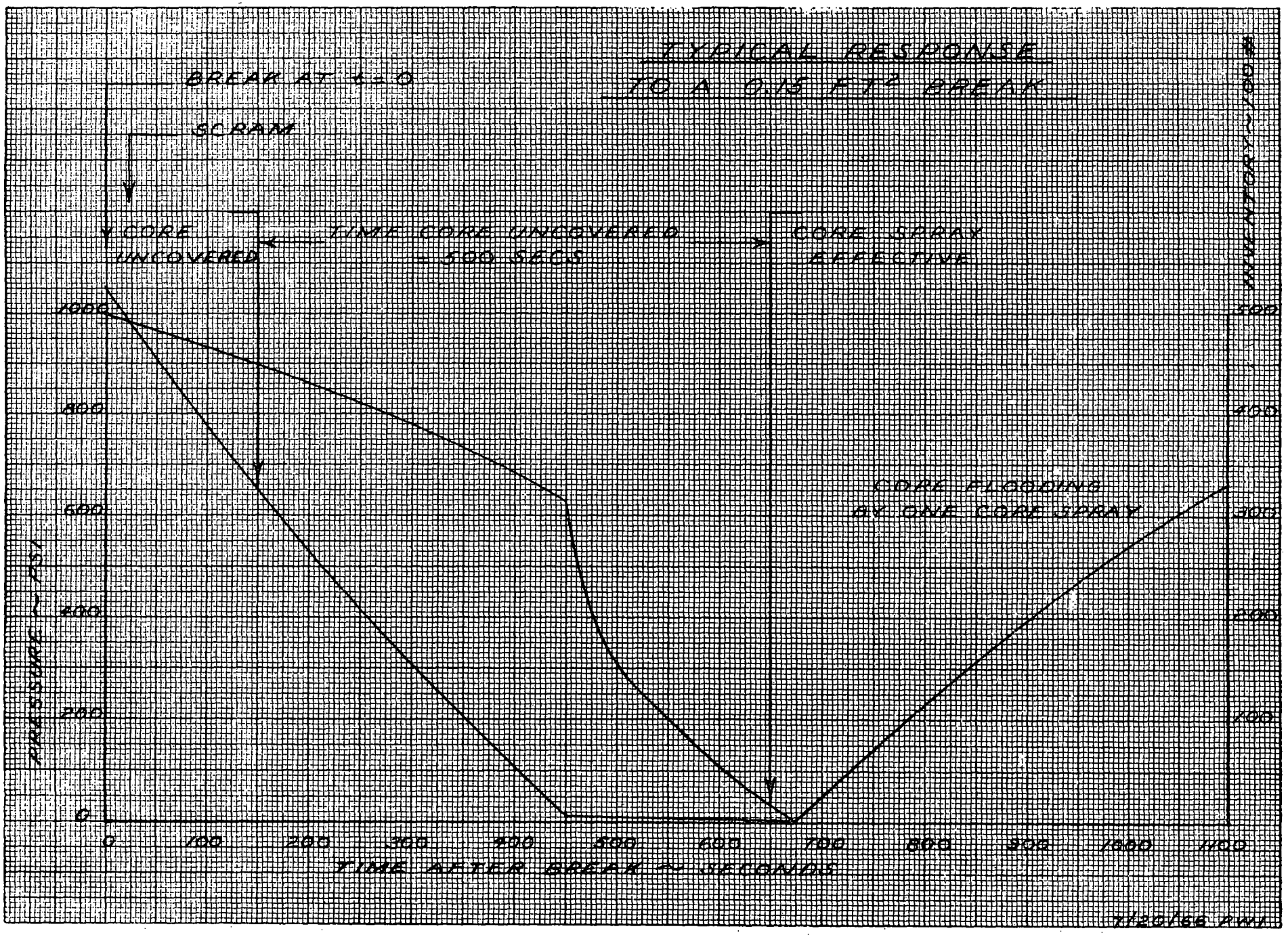

TYPICAL RESPONSE
TO A 0.15 FT² BREAK
BREAK AT t = 0
SCRAM
CORE UNCOVERED
TIME CORE UNCOVERED = 500 SECS
CORE SPRAY EFFECTIVE
CORE FLOODING BY ONE CORE SPRAY
PRESSURE ~ PSIA
1000
800
600
400
200
0
0
100
200
300
400
500
600
700
800
900
1000
1100
TIME AFTER BREAK ~ SECONDS
REACTOR PRESSURE ~ PSIG
500
400
300
200
100
7/20/68 FMI

Fig II 3.10-1

II. 3.11 CORE SPRAY MARGINS

The core spray system, over the range of break sizes that it can protect, has large margins with respect to key parameters that influence its meeting the design objectives. The effects of varying two of the key inputs affecting core spray performance will be evaluated in this section. These are the effects of delaying activation of core spray and the effects of postulating a deteriorated cooling effectiveness within the bundle.

A. Effects of Time Delay

The core spray is actuated by the low-low level signal in the reactor vessel. This signal actuates the core spray pumps and the motor operated valve and is expected to occur within about 10 seconds following the recirculation line break. The level indicators have a cold reference leg so that during rapid blowdown there is no possibility of the level signal indicating in the wrong direction. Vessel pressure will drop to 150 psig in less than 20 seconds. The valve is 50% open by this time thus admitting nearly full flow into the core. Time delay of the fluid reaching the vessel and pump starting times are less than 5 seconds. Hence water should be spraying onto the core at rated flow in less than 30 seconds from the time of the break.

The above represents the actual situation, but for the purpose of showing the margin which exists with respect to any postulated time delay, calculations were performed arbitrarily delaying core spray for various times. After blowdown the core was assumed to be insulated until actuation of core spray. Figure II.3.11-1 shows the effect on the integrated metal-water reaction as a function of core spray delay time. It is seen that even a delay of 240 seconds from the time of the break results in about 1% metal-water reaction which just approaches the flammability limits of the containment.

Figure II.3.11-2 shows the peak cladding temperature reached locally within the core. Figure II.3.11-3 shows a delay of 240 seconds results in about 1/2% clad melting. This represents that portion of the core with local hot spots. It is seen from Figures II.3.11-4 through II.3.11-7 only a few percent of the core volume melts even for delay times in excess of 4 minutes. Therefore, gross core collapse will not occur and the core will maintain its geometry and continued cooling.

From these studies it is concluded that tolerable core spray delay times are of such a magnitude that there is no question that actuation will occur in sufficient time to prevent gross melting or significant metal-water reactions.

B. Effect of Cooling Efficiency

The cooling effectivenss of the core spray has been well demonstrated by actual tests. To determine the tolerance of the system to the cooling effectiveness, the core was examined arbitrarily assuming various degrees of deterioration of the heat transfer coefficient throughout the core.

Figure II.3.11-8 shows the effects on the total metal-water reaction and on the peak temperature reached within the core. It is seen that to reach significant metal-water reaction rates or localized melting a 50% degeneration of the cooling is necessary. This is far in excess of what is expected even for experimental data.

These calculations show, therefore, that the cooling efficiency of the core spray is well in excess of that required to prevent gross core melting or extensive metal-water reactions.

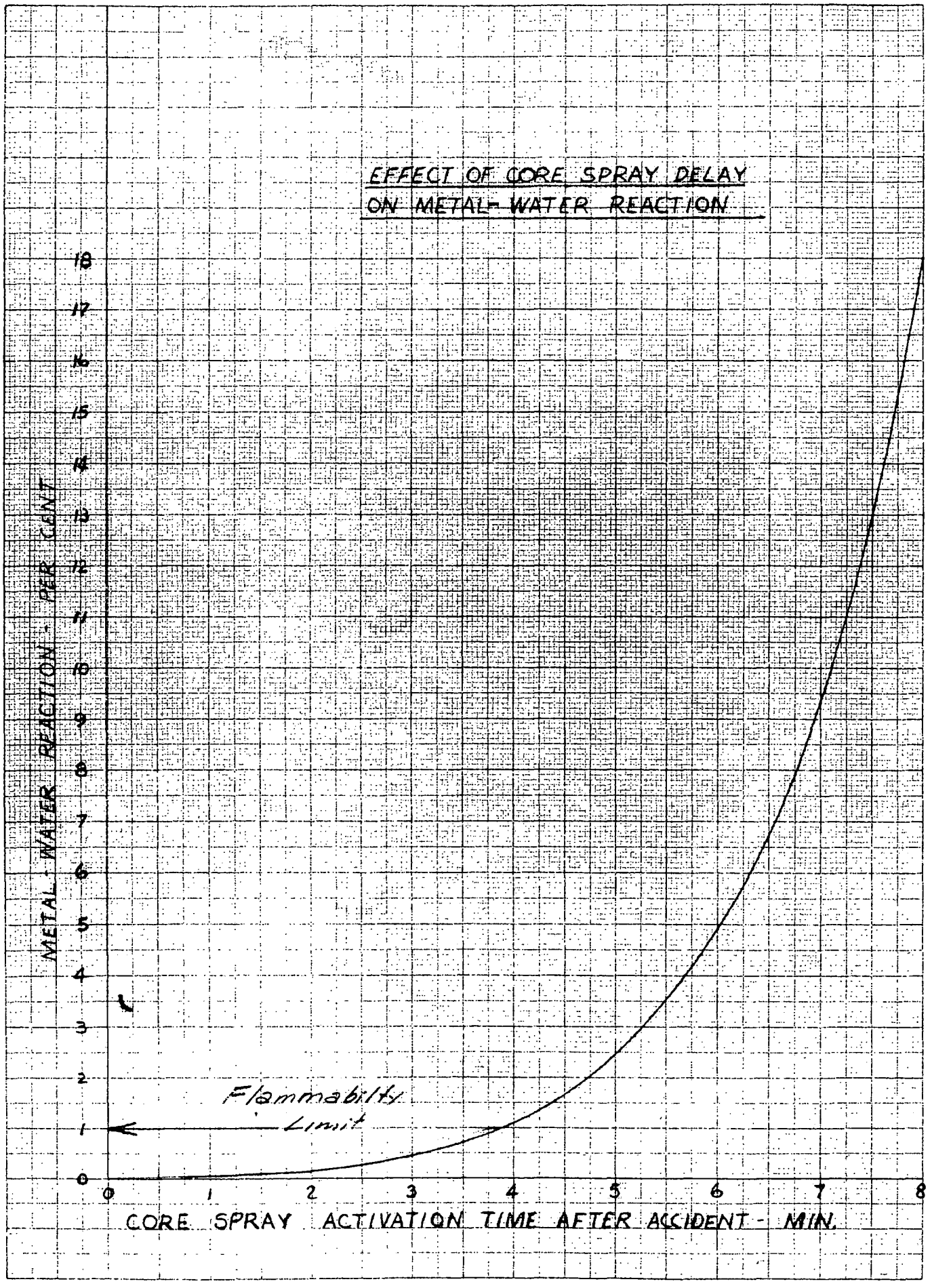


Figure II 3.11-1

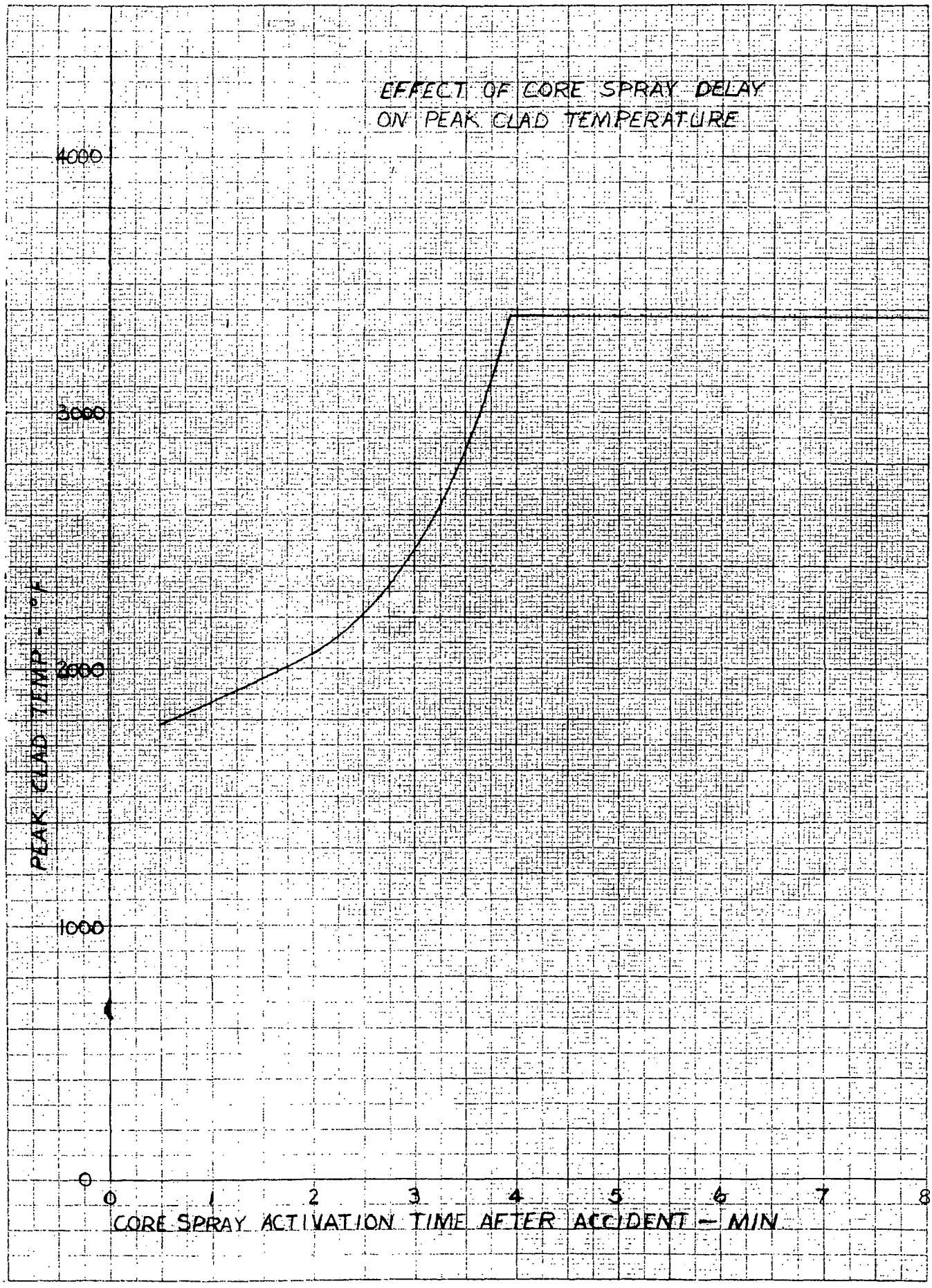


Figure II 3.11-2

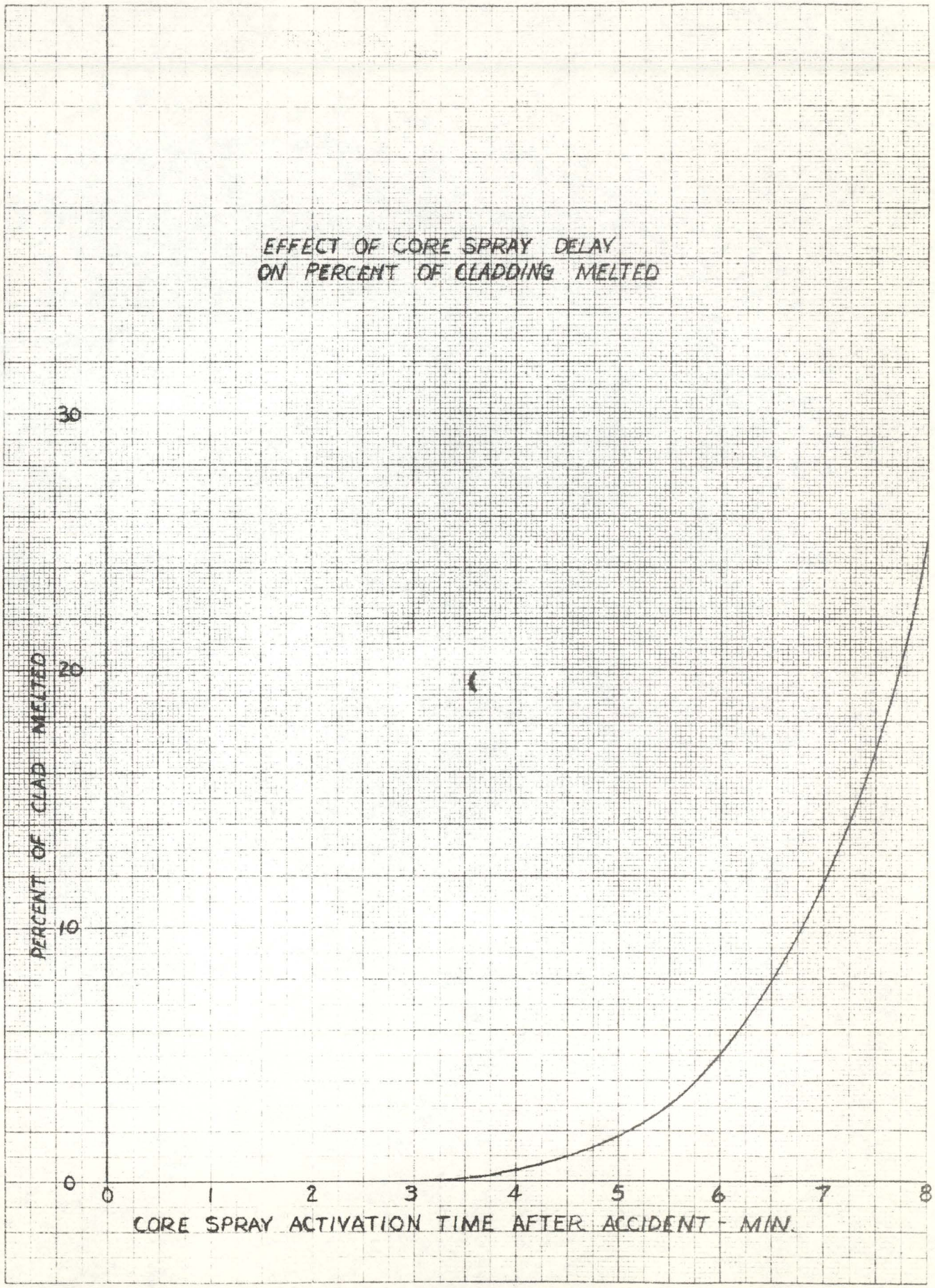


Figure II 3.11-3

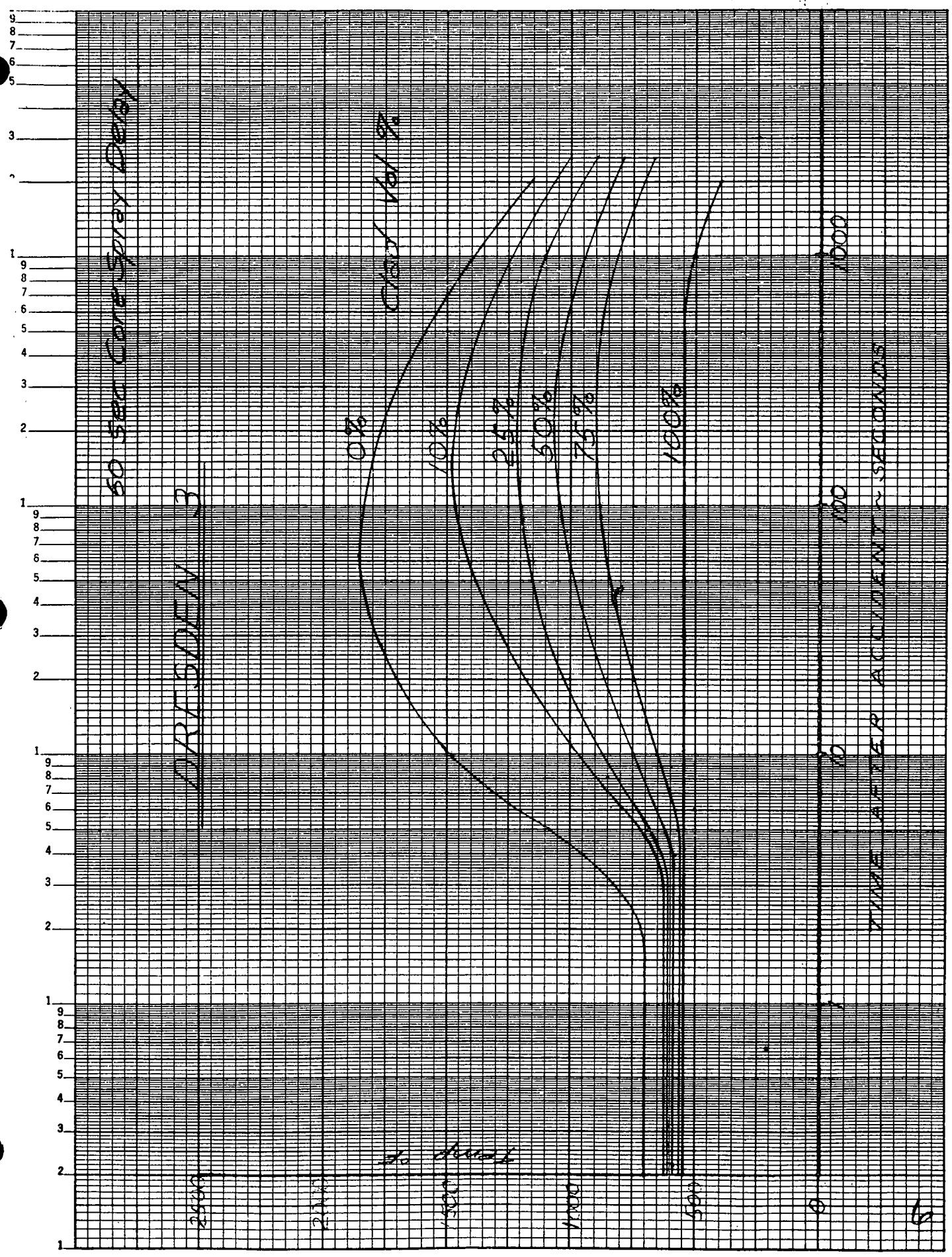


Figure II 3.11-4

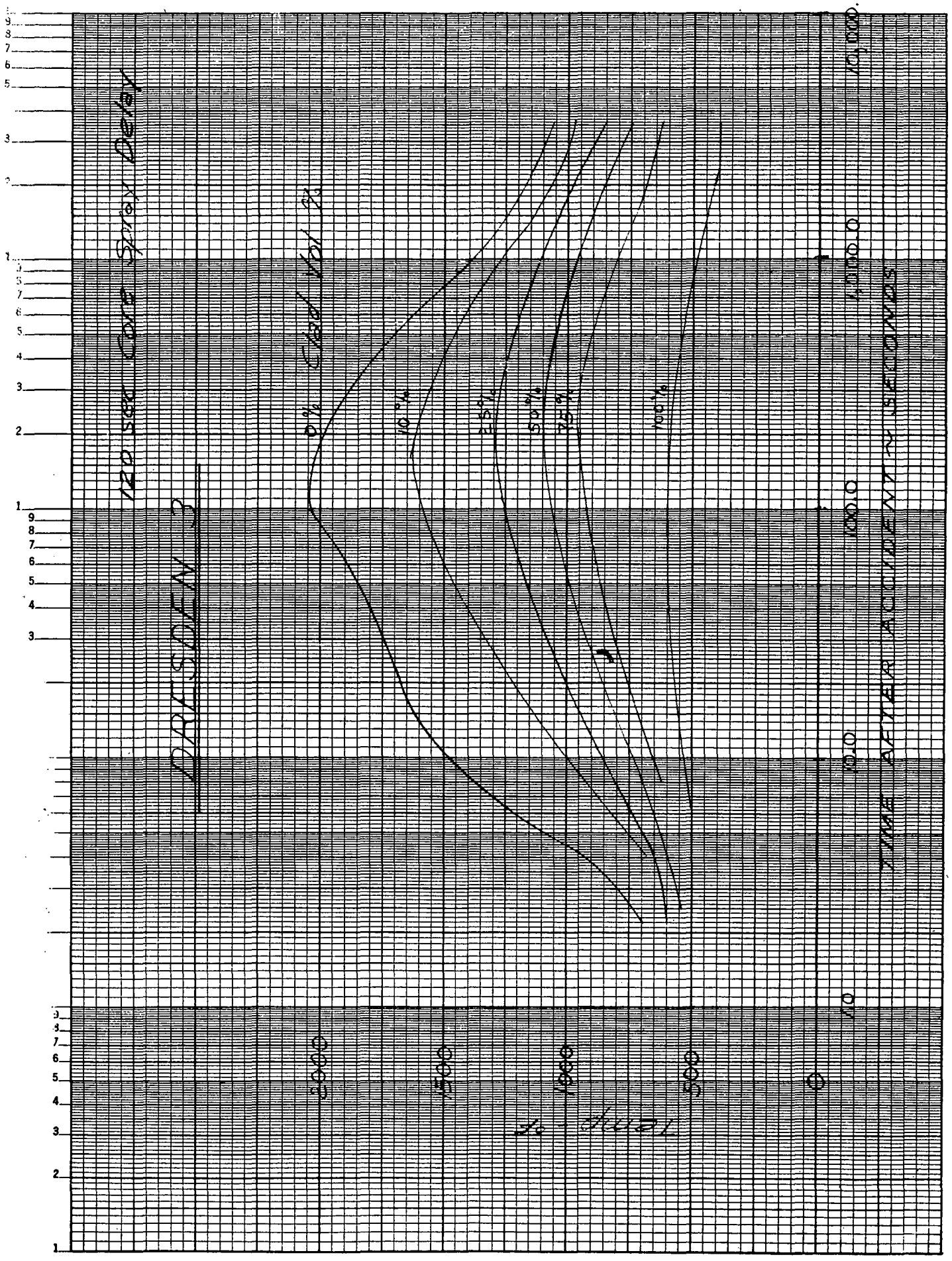


Figure II 3.11-5

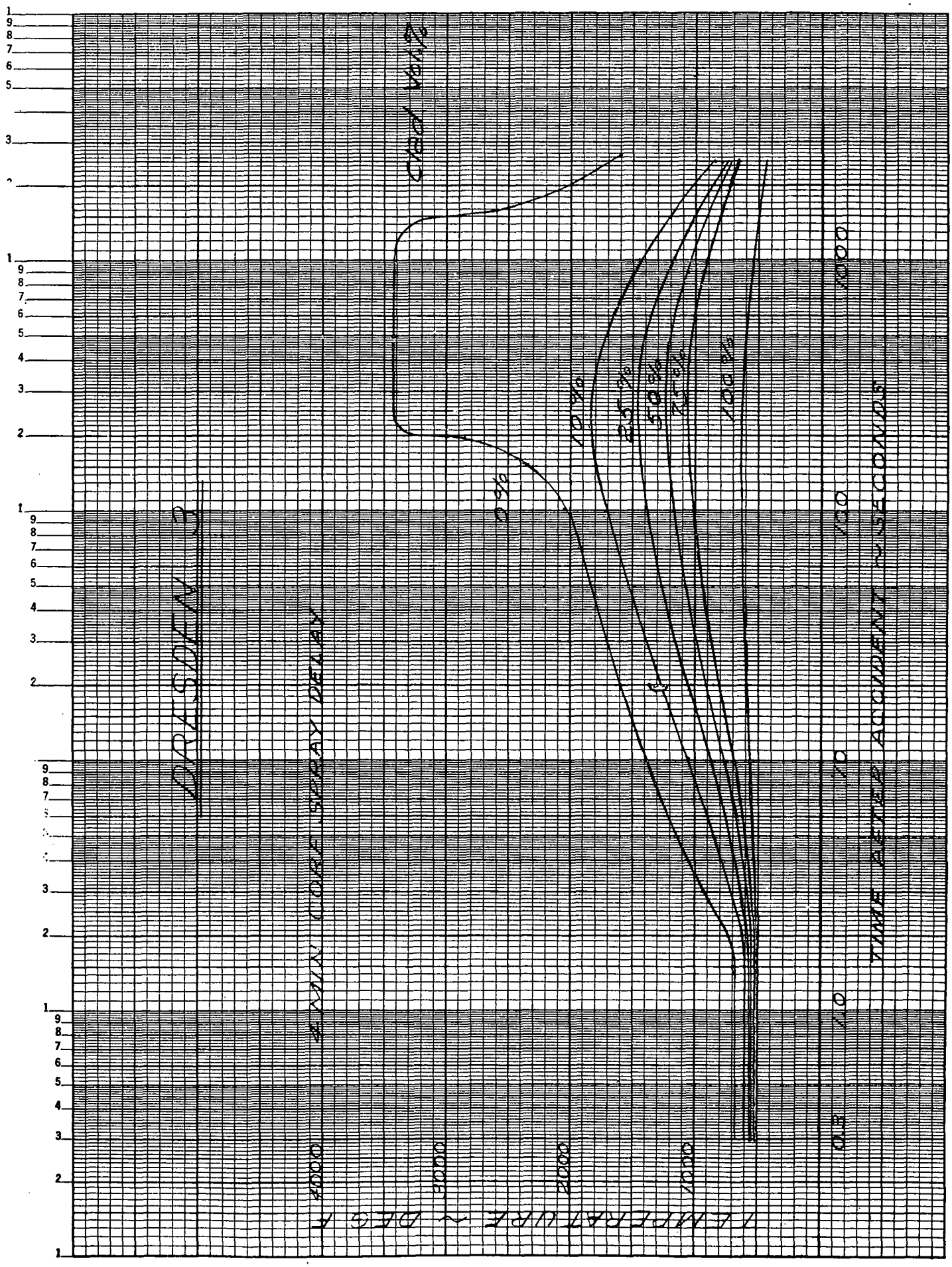


Figure II 3.11-6

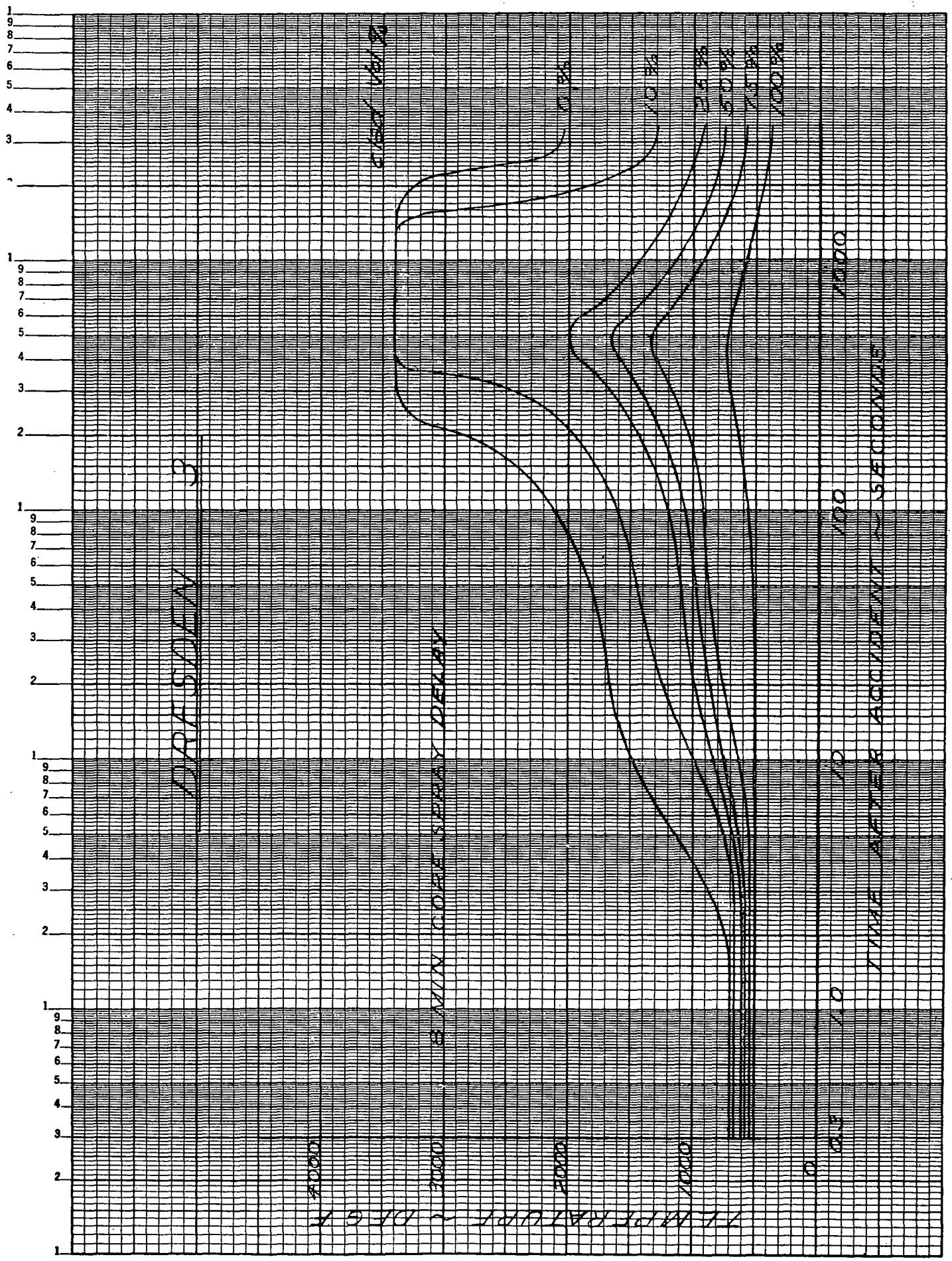


Figure II 3.11-7

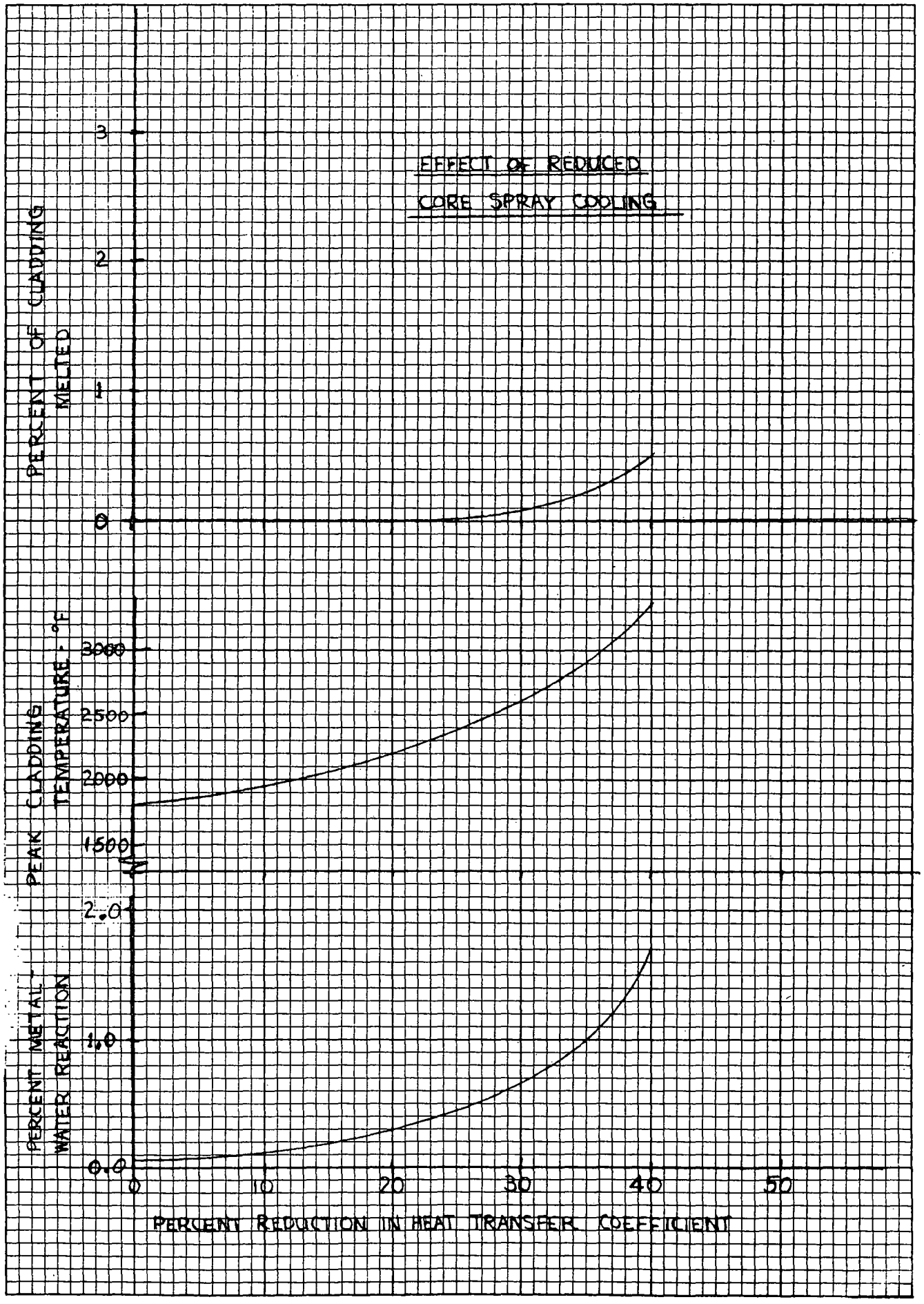


Figure II 3.11-8

II.4 Surveillance and Testing

To assure that the core spray systems will function properly if they are ever required, specific provisions are made for testing the operability and performance of the several components of the systems. Testing will be done periodically at a frequency that will assure availability of the systems. In addition, surveillance features will provide continuous monitoring of the integrity of vital portions of the systems.

The following paragraphs detail the testing that can be done under the different modes of operation of the plant.

Prior to Unit Start-up

a. Prior to plant start-up a pre-operational test of the complete core spray system will be conducted. This test will assure the proper functioning and operation of all instrumentation, pumps, spargers, nozzles and valves. This test will verify that the system meets its design objectives. In addition, system reference characteristics such as pressure differentials and flows will be established at this time to be used as base points for check measurements in the testing to be done during plant operation.

Unit Operating or at "Hot Standby".

a. Flow rate in each loop. The pumps in each loop can be started and full flow established through a by-pass line back to the torus. This provides a test of the availability of the pumps and control circuits of each system.

b. The motor operated gate valves can be exercised and their operability demonstrated.

c. It will be possible to periodically pressurize and test the leak-tightness of the portions of the core spray systems which are exterior to the primary containment vessels.

d. Provisions are made in the design to collect and determine the extent of leakage from the pump seals. Leakage from the valves can be determined by visual inspection.

e. Drain lines on the pump side of the outboard isolation valves provide a check on valve leakage.

Unit Shutdown and Depressurized

a. Flow rate measurements of the entire system with discharge into the reactor vessel. The rate of rise of the water level in the reactor vessel will verify that water is going into the vessel, and the flow-pressure characteristic of each system will be measured and compared to previous measurements to detect any changes. Water supply for this test can be from either the torus or from condensate storage.

Unit Vessel Head and Internals Removed During Refueling

a. Visual inspection of the core spray headers and internal piping.

b. Each core spray system can be operated and flow indication measured at each nozzle. This will verify that nozzles are not plugged. Air may be used for this test to give a visual check that nozzles are clear.

c. Safety and relief valves on the low pressure lines can be removed and tested for set point.

In addition to the periodic tests that are performed, there are continuous surveillance measures that are used to insure the availability of the core spray systems.

a. Pressure differential between each spray header and an internal reference pressure will be continuously monitored during power operation. Changes in these pressure readings will provide indication of loss of integrity of core spray piping within the reactor vessel.

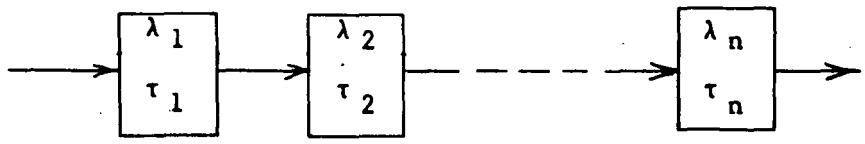
b. Level of water in the suppression chamber will be continuously indicated in the control room.

The control circuitry is described in Section II-B. Included in this description is the action of the control system under test conditions.

APPENDIX II-A

A METHOD FOR ESTIMATING THE AVAILABILITY OF A REPAIRABLE SYSTEM BASED ON THE EXPECTED FAILURE RATES AND REPAIR TIMES OF THE INDIVIDUAL COMPONENTS

If there are "n" subsystems in series with failure rates $\lambda_1, \lambda_2, \text{---} \lambda_n$, and mean repair times $\tau_1, \tau_2, \text{---} \tau_N$, and if repair is instituted on each subsystem as soon as failure occurs, then this



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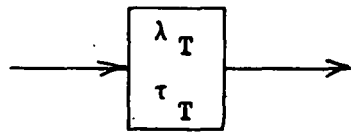


where

$$\lambda_T = \lambda_1 + \lambda_2 + \text{---} \lambda_n$$

and

$$\tau_T = \frac{\lambda_1 \tau_1 + \lambda_2 \tau_2 + \text{---} \lambda_n \tau_n}{\lambda_T}$$

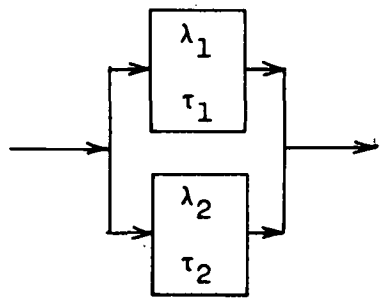
The mean time between failure (MTBF) for the system is given by

$$\text{MTBF} = \frac{1}{\lambda_T}$$

The mean down time is simply τ_T.

If there are two subsystems in parallel with failure rates λ_1 and λ_2 and mean repair times τ_1 and τ_2, and if either one or both subsystems in operation constitute an operating system, and if repair is instituted on each subsystem as soon as failure occurs,

then this



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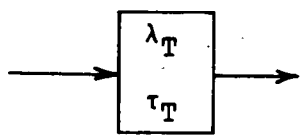


where $\lambda_T = (\lambda_1 \lambda_2)\ (\tau_1 + \tau_2)$

and $\tau_T = \dfrac{\tau_1 \ \tau_2}{\tau_1 + \tau_2}$

The relationship in Appendix IA and IB were derived from "Reliability Prediction for Redundant Systems", S. J. Einhorn, IEEE Proceedings, February 1963, Page 312, and assume that the failures and repairs are randomly distributed and that $1/\lambda >> \tau$.

Availability = $1 - \lambda \tau$

APPENDIX II-B

REACTOR INTERNAL PRESSURE DIFFERENCE MODEL

1. Model & Assumptions

Figure II-3-3-1 shows the typical nodal designation for the five node model. Each constant volume node is assumed to be comprised of liquid and vapor phases in saturated equilibrium or liquid only in subcooled state. Conservation equations of energy, mass and momentum are solved for each node to determine the pressure rate. A maximum of three external flows to each node is permitted in the model. The external flow may be: (a) a function of time, (b) function of enthalpy, (c) critical discharge flow or (d) simple phase supercritical flow.

Major assumptions in the model are:

(a) A node with vapor is assumed to be saturated and in equilibrium with the liquid.

(b) Node volume is constant

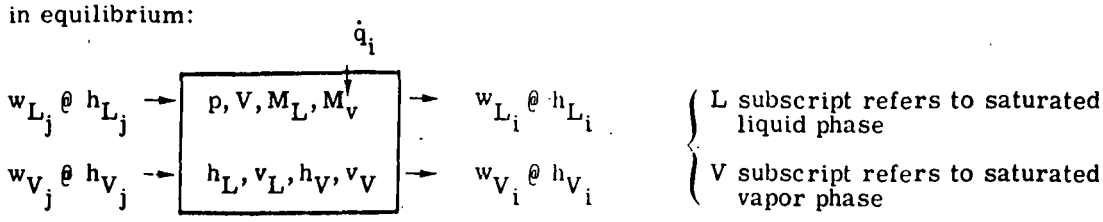
(c) A node is subcooled if the liquid enthalpy is less than that of saturated liquid at the same pressure.

(d) Internal flow between nodes is represented by a single loss coefficient. This coefficient is the same for reverse flow. For two-phase internal flow, a local two phase friction multiplier is used.

1.1 Thermodynamic Equations

a. Saturated Node:

Consider a constant volume node with volume, V_i, and containing saturated liquid and vapor in equilibrium:



$$V_i = v_{V_i} M_{V_i} + v_{L_i} M_{L_i}$$

For a fixed volume, $\dot{V}_i = 0$, and

$$M_{V_i} \frac{dv_{V_i}}{dt} + v_{V_i} \frac{dM_{V_i}}{dt} + M_{L_i} \frac{dv_{L_i}}{dt} + v_{L_i} \frac{dM_{L_i}}{dt} = 0 \tag{1}$$

Or,

$$v_{V_i} \frac{dM_{V_i}}{dt} + v_{L_i} \frac{dM_{L_i}}{dt} + \left\{ M_{V_i} \frac{dv_{V_i}}{dp_i} + M_{L_i} \frac{dv_{L_i}}{dp_i} \right\} \frac{dp_i}{dt} = 0$$

Now, applying conservation of energy to the node,

$$q_i + \sum_j w_{V_{ji}} h_{V_{ji}} + \sum_j w_{L_{ji}} h_{L_{ji}} = \frac{d}{dt} \left[M_{V_i} h_{V_i} + M_{L_i} h_{L_i} \right] - V_i \frac{dp_i}{dt}$$

$$= \left[M_{V_i} \frac{dh_{V_i}}{dp} + M_{L_i} \frac{dh_{L_i}}{dp} \right] \frac{dp_i}{dt} + h_{V_i} \frac{dM_V}{dt} + h_{L_i} \frac{dM_{L_i}}{dt} - V_i \frac{dp_i}{dt} \tag{2}$$

Now, the rate of change of vapor mass in Node i is equal to the net inflow of vapor to the node, plus the rate of mass change from liquid to vapor due to depressurization,

$$\frac{dM_{V_i}}{dt} = \sum_j w_{V_{ji}} + w_{LV_i} \tag{3a}$$

Similarly,

$$\frac{dM_{L_i}}{dt} = \sum_j w_{L_{ji}} - w_{LV_i} \tag{3b}$$

If Equations (3a) and (3b) are substituted into (2) and the resulting expression is solved for w_{LV_i}, the flashing rate, one obtains

$$w_{LV_i} = \frac{\dot{q}_i + \sum_j w_{V_{ji}} \left(h_{V_j} - h_{V_i}\right) + \sum_j w_{L_{ji}} \left(h_{L_j} - h_{L_i}\right) - \left[M_{V_i} \frac{dh_{V_i}}{dp_i} + M_{L_i} \frac{dh_{L_i}}{dp_i} - V_i\right] \dot{p}_i}{h_{LV_i}} \tag{4}$$

From (1),

$$v_{V_i} \left[\sum_j w_{V_{ji}} + w_{LV_i}\right] + v_{L_i} \left[\sum_j w_{L_{ji}} - w_{LV_i}\right] + \left[M_{V_i} \frac{dv_{V_i}}{dp_i} + M_{L_i} \frac{dv_{L_i}}{dp_i}\right] \dot{p}_i = 0$$

Substituting expressing (4) into this equation,

$$\frac{\left(v_{V_i} - v_{L_i}\right)}{h_{LV_i}} \left\{\dot{q}_i + \sum_j w_{V_{ji}} \left(h_{V_j} - h_{V_i}\right) + \sum_j w_{L_{ji}} \left(h_{L_j} - h_{L_i}\right) - \left[M_{V_i} \frac{dh_{V_i}}{dp_i} + M_{L_i} \frac{dh_{L_i}}{dp_i} - V_i\right] \dot{p}_i\right\}$$

$$+ v_{V_i} \sum_j w_{V_{ji}} + v_{L_i} \sum_j w_{L_{ji}} + \left[M_{V_i} \frac{dv_{V_i}}{dp_i} + M_{L_i} \frac{dv_{L_i}}{dp_i}\right] \dot{p}_i = 0$$

By collecting terms and solving for Node i pressure rate, $\dot{p}_i$, the following equation results:

$$-\dot{p}_i = \frac{\left(v_{f_i} - h_{f_i} \frac{v_{fg}}{h_{fg}}\right) \sum_j w_{L_{ji}} + \left(v_{g_i} - h_{g_i} \frac{v_{fg}}{h_{fg}}\right) \sum_j w_{V_{ji}} + \frac{v_{fg}}{h_{fg}} \left(q_i + \sum_j w_{L_{ji}} h_{f_{ji}} + \sum_j w_{V_{ji}} h_{g_{ji}}\right)}{M_{V_i} \left[\frac{dv_{g_i}}{dp_i} - \left(\frac{v_{fg}}{h_{fg}}\right) \frac{dh_{g_i}}{dp_i}\right] + M_{L_i} \left[\frac{dv_{f_i}}{dp_i} - \left(\frac{v_{fg}}{h_{fg}}\right) \frac{dh_{f_i}}{dp_i}\right] + \left(\frac{v_{fg}}{h_{fg}}\right) \frac{V_i}{J}} \tag{5}$$

In this equation, the subscripts f and g have replaced L and V in the thermodynamic properties h and v, denoting that these properties are evaluated from standard steam tables as saturation values. Equation (5) is the equation which is programmed into the RIP program for calculation of saturated node pressure rates. Once the pressure rate is known, the evaporation or flashing rate in the node, w_{LV_i}, is computed from Equation (4). Then the mass rates are calculated from Equation (3), and the entire calculation is updated. This, of course, requires a knowledge of the internal and external flow behavior. The calculation of flow rates is presented in the following sections.

b. Subcooled Node:

The specification of the thermodynamic state of a compressed liquid requires that an additional variable be known with the pressure. If this variable is the subcooled enthalpy of the liquid, the fact that a node has a constant volume can be written as:

$$\frac{d}{dt}\left(vM_i\right) = 0 = v\frac{dM_i}{dt} + M_i\left[\left(\frac{\partial v}{\partial p}\right)_h \frac{dp_i}{dt} + \left(\frac{\partial v}{\partial h}\right)_p \frac{dh_i}{dt}\right] \tag{6}$$

Conservation of energy gives

$$\dot{q}_i + \sum_j w_{ji}\, h_{ji} = \frac{d}{dt}\left(M_i h_{\ell_i}\right) - \frac{V_i}{J}\frac{dp_i}{dt} = h_{\ell_i}\frac{dM_i}{dt} + M_i\frac{dh_{\ell_i}}{dt} - \frac{V_i}{J}\frac{dp_i}{dt}$$

Or,

$$M_i\frac{dh_{\ell_i}}{dt} = \dot{q}_i + \sum_j w_{ji}\, h_{ji} - h_{\ell_i}\frac{dM_i}{dt} + \frac{V_i}{J}\frac{dp_i}{dt} \tag{7}$$

Substituting (7) into (6), combining (dp_i/dt) terms, and solving for (dp_i/dt) will give

$$-\dot{p}_i = \frac{\left[v_{\ell_i} - h_{\ell_i}\left(\frac{\partial v}{\partial h}\right)_p\right]\sum_j w_{ji} + \left(\frac{\partial v}{\partial h}\right)_p\left(\dot{q}_i + \sum_j w_{ji}\, h_{ji}\right)}{M_i\left(\frac{\partial v}{\partial p}\right)_h + \frac{V_i}{J}\left(\frac{\partial v}{\partial h}\right)_p} \tag{8}$$

Equation (8) is the expression used in the RIP program to compute the pressure rate in a compressed liquid node. Since the liquid enthalpy must be known at each time to compute the pressure rate, it must similarly be updated. Equation (7) gives the rate of enthalpy change with time,

$$\frac{dh_{\ell_i}}{dt} = \frac{1}{M_i}\left[\dot{q}_i + \sum_j w_{ji}\, h_{ji} - h_{\ell_i}\frac{dM_i}{dt} + \frac{V_i}{J}\frac{dp_i}{dt}\right] \tag{9}$$

The pressure rate at each time is calculated first, and then (9) is used to calculate a new value of liquid enthalpy. Internal and external flow rates for a subcooled node are calculated in the same manner as for a saturated node. However, the energy content of flows out of a subcooled node will be equal to the compressed liquid enthalpy value.

1.2 Internal Flow Calculation

The instantaneous flow between Node i and Node j is taken as being in the direction of the instantaneous pressure difference between the two nodes. The magnitude of the flow rate is computed from a single lumped resistance value which is input by the user.

$$w_{ij} = \frac{\phi_1}{\phi_n R_{ij}} \sqrt{\frac{P_i - P_j}{v_{f_i}}} \tag{10}$$

$$\text{where } R_{ij} \equiv \frac{1}{w_{ij}} \sqrt{\frac{P_i - P_j}{v_{f_i}}} \quad \text{evaluated at initial time}$$

The specific volume, v_{f_i}, is the value for the node from which the flow is coming. Its value is determined by a table lookup.

Saturated thermodynamic properties for use in Equations (5) and (10) and subcooled liquid properties for water for use as Equations (8), (9) and (10), are contained within the program.

Currently, there is no distinction between forward flow and reverse flow through a resistance between nodes, i.e., for all internal flows,

$$R_{ji} = R_{ij}$$

1.3 External Flow Calculation

Specification of external flows into or out of the reactor vessel is the means by which a particular accident is described from one of the following types of external flow:

a. The flow is a function of time

b. The flow is a function of enthalpy

c. The flow is critical discharge flow.

Critical flow is flow which is "choked" at the point of minimum flow area in the line which is ruptured. Critical or maximum two-phase flow will occur if the ratio of driving pressure (node pressure) to sink pressure is greater than approximately two. When critical discharge flow is specified, the results of the critical two-phase flow investigation presented in Reference 2 are used within the program to compute a critical flow mass rate, G_c:

$$G_c = G_c \quad (P, h, \frac{fL}{D})$$

After the critical flow mass rate, G_c, has been interpolated within the program, the total flow rate is calculated from

$$w_c = G_c A_B$$

where A_B is the minimum flow area in the line in which the break occurs.

d. The flow is supercritical flow

Supercritical flow is flow which exists prior to the formation of bubbles and establishment of two phase critical flow. The user inputs the time at which supercritical flow is to be switched to

critical flow. The supercritical calculation will continue until this input switching time is reached. Supercritical flow is calculated as follows:

$$G_{scrit} = \sqrt{\frac{2_g\ (144)\ (P_i - P_{sink})}{v_{f_i}\ (\frac{fL}{d} + 1.4)\ \Phi^2}}$$

and, $_{scrit} = G_{scrit}\ A_B$

1.4 Critical Flow Calculation for Subcooled Nodes

The critical flow tables in RIP are based on a model which assumes the liquid in the node is in a saturated state. For a subcooled liquid blowdown through a pipe, the location of the saturation point in the pipe must be located, and the critical flow is then based on the coolant condition at this point. If the pipe is sufficiently short (such as a nozzle) such that a saturation state is not reached, then the critical flow is assumed to be limited to a saturated liquid blowdown with (fL/D) = 0. For longer pipes, or rather for pipes with sufficiently large (fL/D) values, the pressure drop along the pipe is computed for an assumed value of critical mass flow rate, G_c, and the required pipe length to reach a saturated condition is computed based on the node stagnation enthalpy, h_o, which is assumed to remain constant. The critical discharge flow based on this local pressure and the remaining pipe friction length is then determined from the critical flow subroutine, and this value is compared with the assumed value. If these two values are not equal within a specified margin the flow is updated, and the procedure is repeated until a converged value is obtained. The sequence of calculations can be summarized as follows:

a. assume $G_c = G_c\ (p_o,\ h_o,\ \frac{fL}{D} = 0)$

b. from input tables, the saturation pressure at the stagnation pressure is determined,

$$p_{DUM} = p_{SAT}\ (h_o)$$

c. The friction length required to reach the saturation pressure, p_{DUM}, is calculated from

$$\frac{fLsc}{D} = \frac{(p_o - p_{DUM})\ 144}{\left(\frac{v_\ell}{2g}\right) G^2}$$

The critical flow based on the local pressure at the point of saturation and the remaining pipe friction in length is determined,

$$G_c = G_c \left[h_o,\ p_{DUM},\ \left(\frac{fL}{D} - \frac{fLsc}{D}\right)\right]$$

If the above G_c value does not agree with the assumed value within 100 lbs/(sec) (ft^2), a new flow rate is calculated,

$$G_{new} = \frac{G_{assumed} + G_{calculated}}{2}$$

and steps 1) through 4) are repeated until a converged G_c is obtained.

1.5 Momentum Calculation

A more accurate model is obtained by including momentum effects during a flow reversal and during the initial acceleration of supercritical and critical flows. For internal flow the following equation is used to calculate the change in mass flow rate.

$$\frac{dw_{ij}}{dt} = \frac{144\, g A_{ij}}{L_M} \left[- R^2_{ij} \frac{\Phi^2_n}{\Phi^2_1} v_{f_i} \; w_{ij} \left| w_{ij} \right| + P_i - P_j \right]$$

If flow length, L, is zero, the internal momentum calculation is bypassed. Likewise, the external momentum calculation is bypassed if the external pipe length, L, is zero. When external momentum is calculated the following equation is used:

$$\frac{dG_{io}}{dt} = \frac{g}{L_M} \left[- K_{EM}\, G_{io} \left| G_{io} \right| + 144\, (P_i - P_{sink}) \right]$$

where $K_{EM} = \frac{\Phi^2_n}{2g} v_{f_i} \left(\frac{fL_M}{D} + \sum K_{local} \right)$

and $K_{local} = \frac{2g\, \Delta P_{local}}{v_f\, G^2}$

In each case, the momentum calculation is continued until $\frac{dG}{dt} \leq$ or $\frac{dw}{dt} \leq Kw$. The factor K is input by the user.

1.6 Quality Calculation

In general the exit quality of node i is calculated by

$$x_i = \frac{M_v}{M_v + M_L}$$

which assumes the node is an homogeneous two phase mixture. However, in the core node, this is not applicable because the quality is not uniform throughout the node. Instead of basing the quality calculation on the mass inventory, the following equation is used for the core node when flow enters the core from the lower plenum:

$$x_i = \frac{x_{ij} \quad (+W_{ij}) + W_{LV_i}}{(+W_{ij})}$$

where $+ W_{ij}$ are the flows into i from j.

If an inlet flow reversal occurs, i.e., flow enters the lower plenum from the core, the core exit quality is then calculated using the mass inventory equation.

2. Comparison to Experimental Data

A series of blowdown tests were conducted at APED with a 36-rod heated bundle test section to determine actual pressure responses during a loss-of-coolant accident. The transient pressure response measurements obtained from these tests have been favorably compared to the computer code calculated results and are discussed in later sections of this report.

2.1 Test Section Description

A schematic of the test section is shown by Figure II-B-1. It is comprised of three distinct pressure vessels which are connected by piping. Vessel 1 as shown in Figure II-B-1 represents a lower plenum which contains subcooled liquid prior to a blowdown. This vessel is connected through piping and some orifice resistance to the 36-rod heated bundle section. The subcooled water entering the bottom is heated and exits through the top piping as a two-phase mixture and enters the top vessel, which represents a reactor upper plenum. The mixture flows out of the vessel through the top discharge line as shown. The locations of the rupture discs which initiate the blowdown are identified in Figure II-B-1. These rupture discs are placed in one-inch lines, one of the lines being connected to the lower subcooled plenum and the other being taken off the steam volume in the upper plenum.

The top plenum break roughly simulates a steam line rupture since steam only is considered to be discharged initially out of the top rupture line in the tests. For jet pump plants, it also represents the recirculation line break since the steam dome and mixing plenum are being depressurized as would be the case for a "recirculation break".

mixing as the pressure falls. This transition from steam flow to mixture flow is assumed to occur at ten seconds.

It should be noted that the purpose of this comparison is to illustrate the capability of the analytical model to simulate the blowdown experimental data. The experimental data is not representative of pressure forces developed in a jet pump plant. The flow resistances used in the test section are much higher than plant resistances.

Figure II-B-2 shows the comparison of absolute pressures for this case. The comparison of experimental and calculated behavior can be summarized as follows:

1. The model may underpredict the critical flow rate of steam and consequently the calculated pressure responses are initially somewhat higher than measured. It has been determined that the reason for the low calculated critical flow values is the manner in which they are interpolated within the RIP program.
2. The pressure difference comparison (Figure II-B-3) is favorable with regard to shape of the curve and maximum value. With the proper calculated flow rates, the comparison here should also improve considerably.
3. The pressure response in the upper plenum is dependent on the value of fl/d in the blowdown line. Since the rupture discs are located upstream of the line exit, the value of fl/d changes as the line fills during initial blowdown. The approximate function used in the code input is better for short term results and results in some error for long term results.

The comparison of the analytical model to the test section data demonstrates the validity of basic model. The analytical curve exhibits the proper shape and closely predicts the maximum pressure difference. The comparison is sufficiently good to warrant confidence in the analytical results.

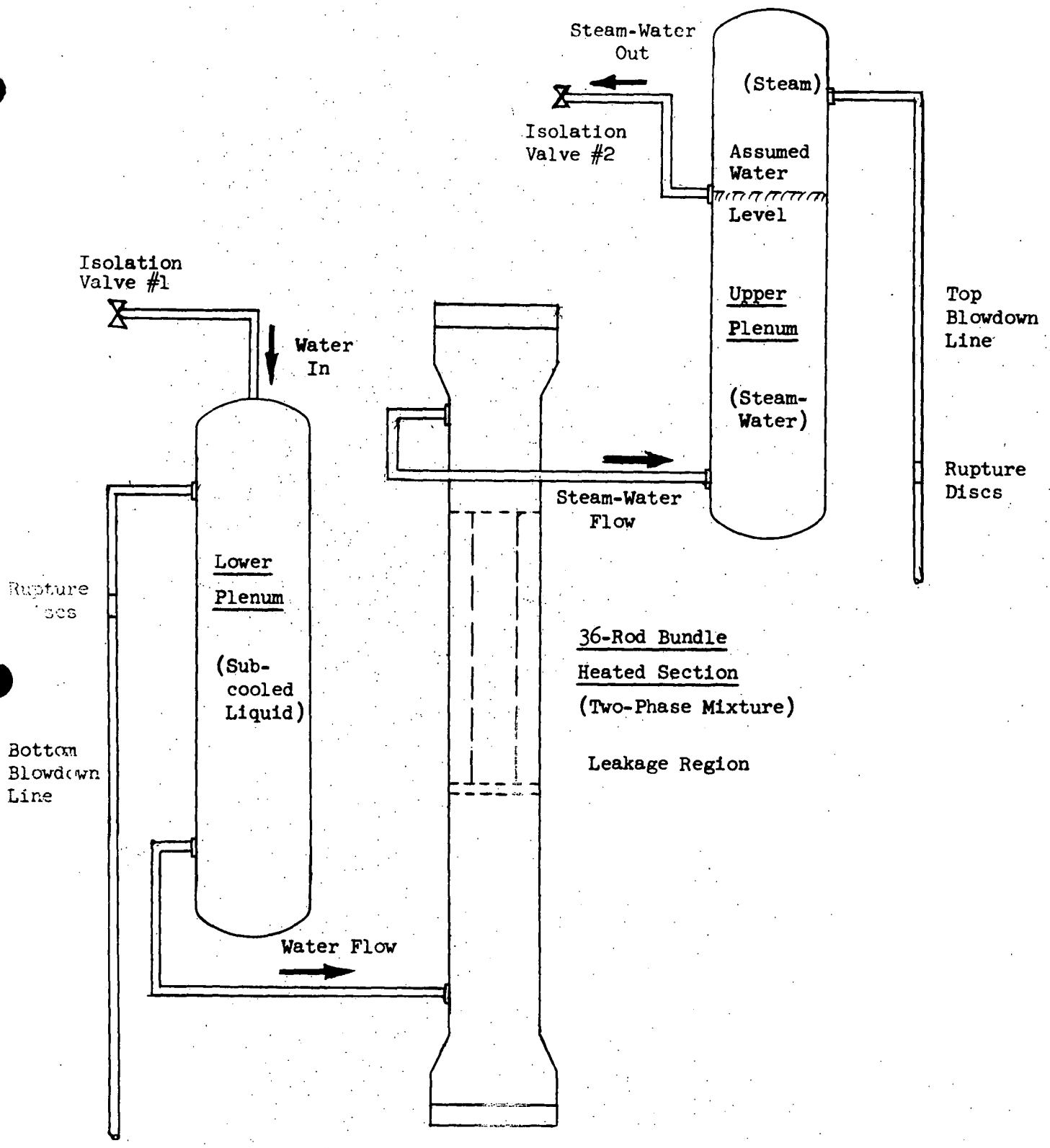


FIGURE II-B-1 SCHEMATIC OF APED LOSS-OF-COOLANT TEST SECTION

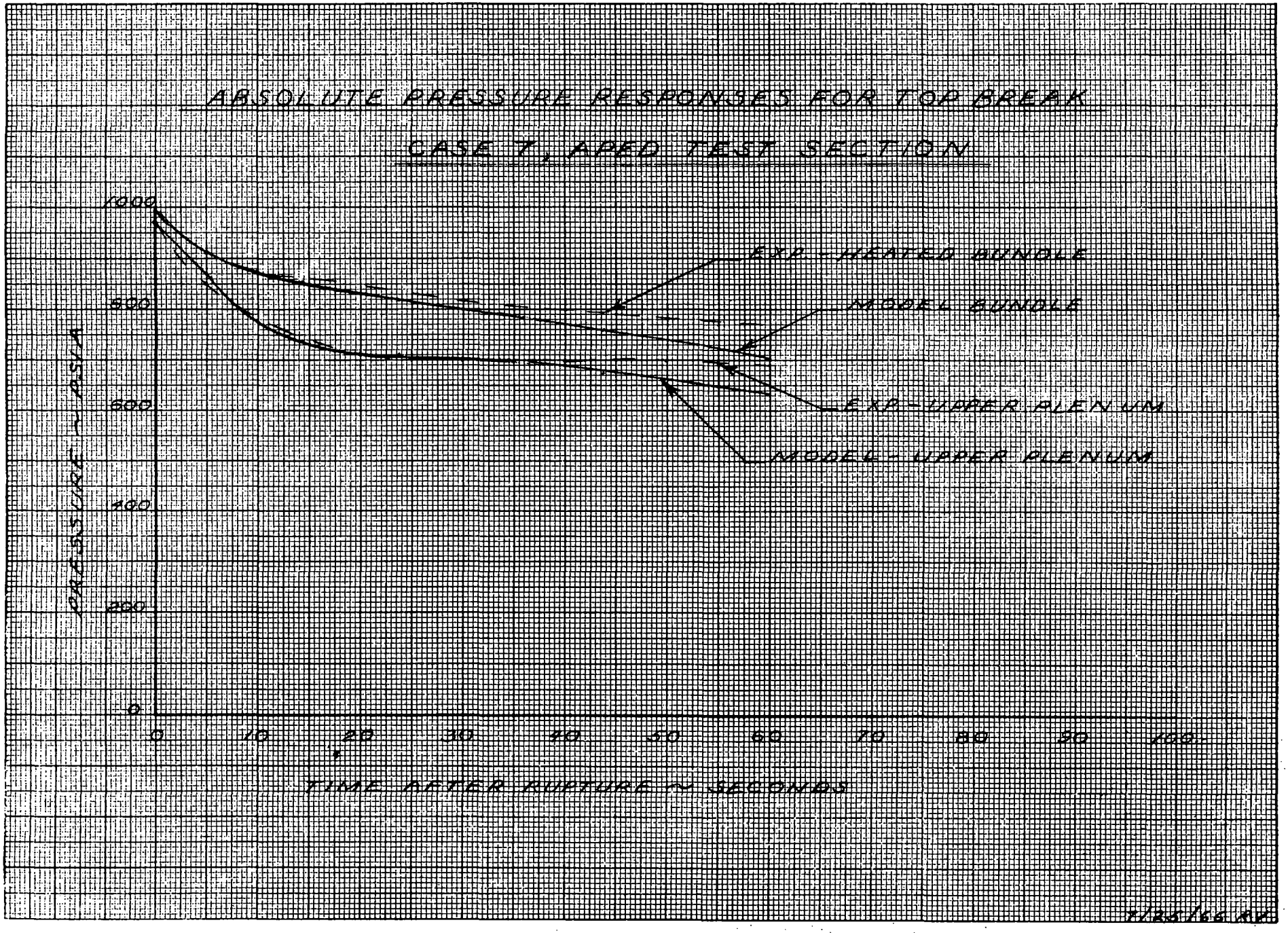

ABSOLUTE PRESSURE RESPONSES FOR TOP BREAK
CASE 7, AREO TEST SECTION
EXP - HEATED BUNDLE
MODEL BUNDLE
EXP - UPPER PLENUM
MODEL - UPPER PLENUM
PRESSURE ~ PSIA
1000
800
600
400
200
0
0
10
20
30
40
50
60
70
80
90
100
TIME AFTER RUPTURE ~ SECONDS

Figure II-B-2

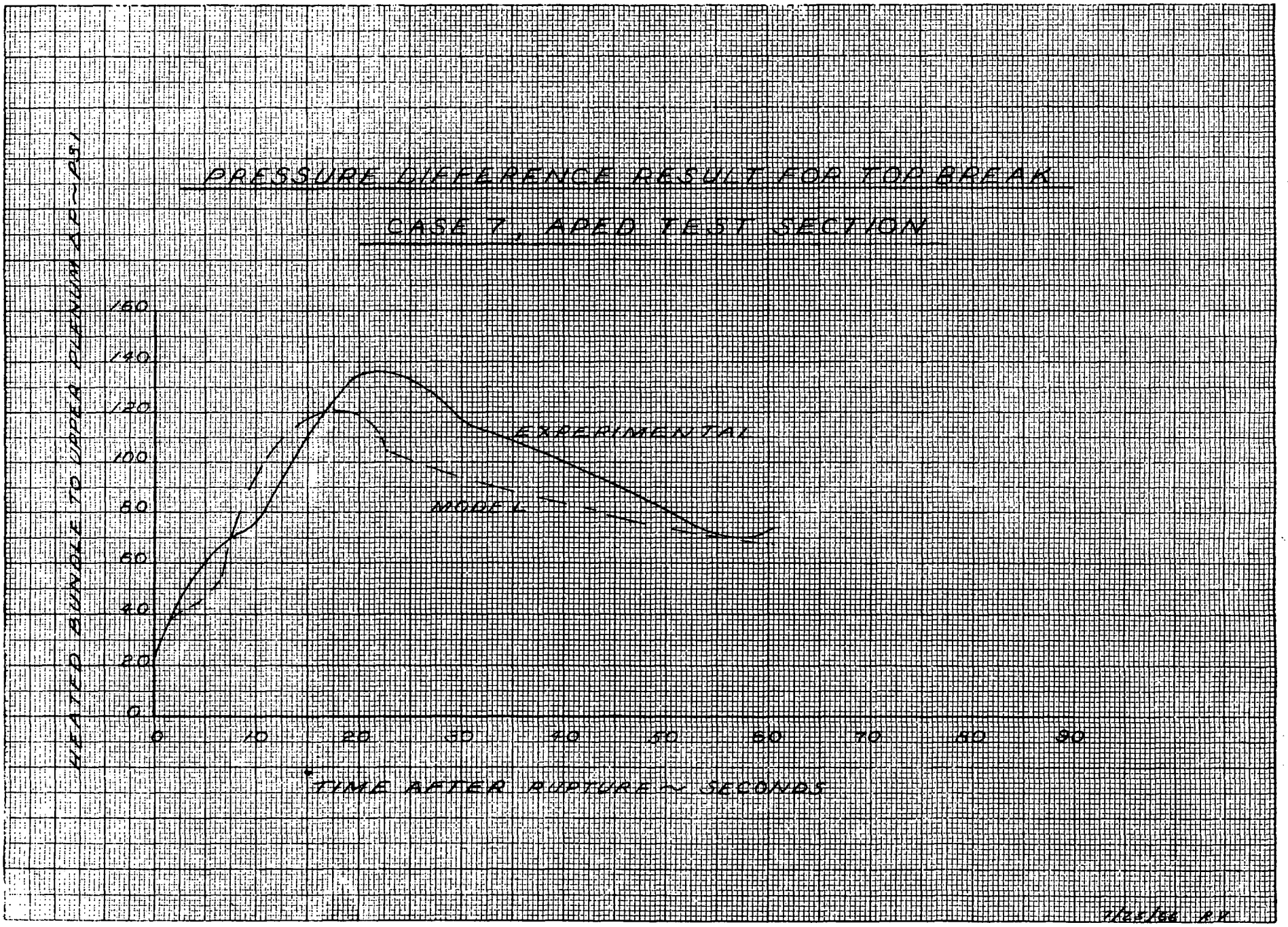

PRESSURE DIFFERENCE RESULT FOR TOP BREAK
CASE 7, APED TEST SECTION
HEATED BUNDLE TO UPPER PLENUM ΔP ~ PSI
160
140
120
100
80
60
40
20
0
0
10
20
30
40
50
60
70
80
90
EXPERIMENTAL
MODEL
TIME AFTER RUPTURE ~ SECONDS

Figure II-B-3

APPENDIX II-C

CORE SPRAY TEST PROGRAM

This test program was undertaken to determine the effectiveness of reactor core spray systems. A spray distribution facility which simulates the core spray ring and nozzles and fuel assemblies was employed to determine the amount of spray reaching each assembly in a core. A second facility consisting of a prototype fuel assembly with electrically heated rods thoroughly instrumented with thermocouples and a channel box simulated the reactor conditions to determine the effectiveness of core spray cooling. Finally, a third phase of this test program determined the degree of core covery required for adequate cooling.

1. Core Spray Distribution

Figure II-C-1 is a schematic of the pool test setup. An array of 25 short channels can be moved to predetermined positions on the floor of the pool, corresponding to different channel positions across the core. The ring of spray nozzles can be raised or lowered with respect to the elevation of the top of the channels. The tops of the channels are complete with production fuel assembly handles and oriented as they are in the reactor. The nozzle angles can be adjusted. A sampling tube is attached to one of the channels and passes through the floor of the pool. When the spray is on, the rate at which spray enters the particular channel can then be measured.

Typical results with "Full Jet" nozzles (Z=6", θ=15°) are shown in Figure II-C-2. The ordinate is the flow per channel, normalized by dividing by the "ideal" flow per channel. This quotient is called the distribution factor. This is plotted along one of the diameters, to show a profile of the distribution across the core. Note the flow-deficient region between the center and the outside, which extends as a circular band clear around the core. Spray distribution is improved when the minimum flow in this depressed region is increased. From an examination of Figure II-C-2 it is seen that the minimum flow per channel is approximately 35% of the average flow across the core.

Although this distribution was typical of many of the veins, the insight developed in observing the testing of the various parameters, led to the conclusion that a 40% distribution can be achieved in the Dresden Unit 3 reactor with some design changes in the nozzle arrays or arrangement. This is particularly true in view of the fact that the tests were in the open without a cover and some of the water that splashed off was not recovered as would be the case inside the upper plenum.

Analytical methods have also been developed which approximate the spray distribution observed in the prototype tests. These models have also been used to design the Dresden Units 2 and 3 core spray systems.

2. Spray Effectiveness

The 36 rod electrically heated test section is shown in Figure II-C-3. The calrods were specially made to have a cosine axial power distribution. Eight of the rods were supplied with an incrementally higher voltage to simulate flux peaking. Eight of the rods were instrumented with twelve thermocouples each, distributed along the length of the rod. The channel was also instrumented with thermocouples. The position of the instrumented rods and also the flux peaking rods, are shown in Figure II-C-3. During the tests, rods B, C, E, F and H generally ran the hottest, not unexpectedly. Thermocouples of rod G, E and C, in combination with channel thermocouples, yield the transverse temperature profile.

The test was started by first heating the assembly with no cooling. When the highest rod temperature reached 1250°F (channel temperature about 650°F) the spray was turned on and the power then varied as a function of time to simulate decay heat. The channel temperature traces indicated an abrupt drop in temperature about 100 seconds after the onset of spray due to wetting the top half of the channel wall. It is estimated that about this much more time is required for the remainder of the channel wall to be wetted. From that time on, the wall acts as a heat sink. The rod temperatures continue to rise eventually reaching a maximum and then decrease, finally reaching saturation temperature some 20 to 40 minutes later, depending on the power.

The peak temperature occurring during the transient following onset of spray is plotted versus full channel power in Figure II-C-4. Tests were run with provisions for venting at both top and bottom or top only, at different initial rod temperatures, and over a range of flows from 1.8 to 2.8 gpm. None of these parameters had a strong effect on the peak temperature. The negligible effect of flow noted is believed to be due to the strong radiation of heat at the temperature experienced. Radiation is a function of temperature and is unaffected by flow rate.

It must be understood that the phenomena occurring within the fuel bundle is not one of rod wetting, nor pure convection, nor pure radiation. It is a complex combination in which radiation plays a key role. Because of the "Liendenfront Effect" the water is thrown off the rods near the top of the bundle and onto the channel wall. The rest falls between the rods sputtering and splashing between them. A considerable amount of water runs down the fuel

channels around the rod array, lowering off of the spacers and back into the array every 18". The channel walls remain wet and act as a heat sink for thermal radiation. Some of the water which finds its way into the fuel rods evaporates, forming steam which provides some convection cooling. Only a quarter or so of the water actually evaporates, depending on the point in time. Radiation takes place between rod surfaces to the water vapor, and to the water droplets, and to the channel walls.

As the decay heat slowly drops the heat flux is reduced to the point where rod wetting actually occurs, since the Liendenfront Effect no longer is present, and rod temperature drops to saturation (~220°F). This occurred in the tests which simulated decay heat within 40 minutes to an hour. However, since the core is essentially flooded in about 10 minutes following the design break, the temperatures throughout most of the core reach saturation much sooner.

Of importance in considering any possible chemical reaction between clad metal and spray water is the time at temperature, shown in Figure II-C-5. These data are for just one test run. A similar plot can be prepared for each of the other test runs. The top curve represents the highest temperature experienced by the hottest single point on the surface of all the rods. The label "0%" signifies that there was no surface at a higher temperature. The second curve represents the highest temperature experienced by all but 25% of the total surface, and so on. The extent of any metal-water reaction is a function of the temperature, time, and extent of surface.

The core heatup calculational model has built into it radiation between rods and to the channel walls along with calculated form factors and emissivities. Hence it accounts for a good portion of the radiation, but not all of it, i.e., that to the vapor and droplets. Good agreement with the test temperature data was obtained by emperically changing the convection coefficient in the model. Hence this is called an "effective heat transfer coefficient" since it is really the vehicle through which the test data was matched to the model. Therefore, this coefficient takes into account all those factors not built into the model analytically (such as radiation to the steam) as well as all other approximations, lack of knowledge, etc. which are inherent in any model.

Typical analysis using this combined radiation and convection model predicted the "time at temperature" curves. A typical curve is shown in Figure II-C-6, together with data points. The model includes an effective heat transfer coefficient (h) which has here set at a constant value of 3.6 Btu/hr ft^2°F to make the curve agree with the data. Similar good agreement has been obtained

for the highest power run of the series with "b" set at 3.9 Btu/hr ft^2°F.

These "effective heat transfer coefficients" were correlated as a weak function of initial fuel temperature at the time core spray first entered, and approximately as the power level of the fuel rods. The coefficients increase with both power and initial temperature but the latter is not a strong function.

3. Core Flooding for Effective Cooling

Because the top of the jet pumps are 2/3 the height of the core, it is possible to flood the core to that level. One core spray can fill the core to this elevation within about 5 minutes. Tests show that the core is adequately cooled under such conditions. Thus, even if the core spray were completely ineffective, gross core meltdown would not occur as long as core spray water can enter the shroud.

As shown in Figure II-C-7, tests were conducted to determine the cooling effectiveness as a function of the flooded height as measured by an external manometer which essentially corresponds to the jet pumps. The tests were conducted on full scale assemblies with uniform heat generation. Steam generated in the flooded portion of the fuel causes frothing and subsequent rising of the actual level within the channel thus cooling the rods. Table II-C-1 summarizes the test results.

TABLE II-C-1

Flooded Tests

			Thermocouple Elevation (ft.)					
*Height of Water (ft.	Time After Scram Hrs. (Avg. Bundle)	Power (KW)	1	3	5	7	9	11.5
			Temperature °F					
4	40.	15.	215	215	540	790	920	910
	10.	30	215	215	215	965	1220	1300
	0.4	60	215	215	215	750	1090	1290
	0.1	90	215	215	215	215	945	1220
6	40	15	215	215	215	215	945	1160
	0.4	60	215	215	215	215	245	1025
	0.1	90	215	215	215	215	215	970
8	40.	15	215	215	215	215	580	1150
	10.	30	215	215	215	215	560	1150
	0.4	60	215	215	215	215	215	1075
	0.1	90	215	215	215	215	215	215

*As indicated by external menometer.

Most of the data of interest with respect to the performance of the Injection Cooling System lies in the 0.1 hour range. As seen in the table the maximum temperatures in this time span for the core effectively half covered lie below, or very near, 1000°F. Thus, when the core is "half covered" over 3/4 of the core is at saturation condition and only a short section at the top will be near the 1000°F temperatures.

4. Application to Reactor Core

When reduced, the data indicates that at the minimum flow substantiated by the tests, a flow of 0.05 gpm is required per rod. To obtain the required core spray flow rate for a given core it is necessary to multiply the total number of rods by the 0.05 gpm and divide the result by the minimum distribution factor expected for this design. The minimum distribution factor of 0.40 represents the most severe situation that is anticipated for the actual reactor configuration and was therefore used to establish core spray rates.

To determine the time at temperature following initiation of core spray, the effective heat transfer coefficient correlation as determined by these experiments was used as the input to the TACTV core heatup code, thus insuring that the temperature time history would be the same as that determined experimentally.

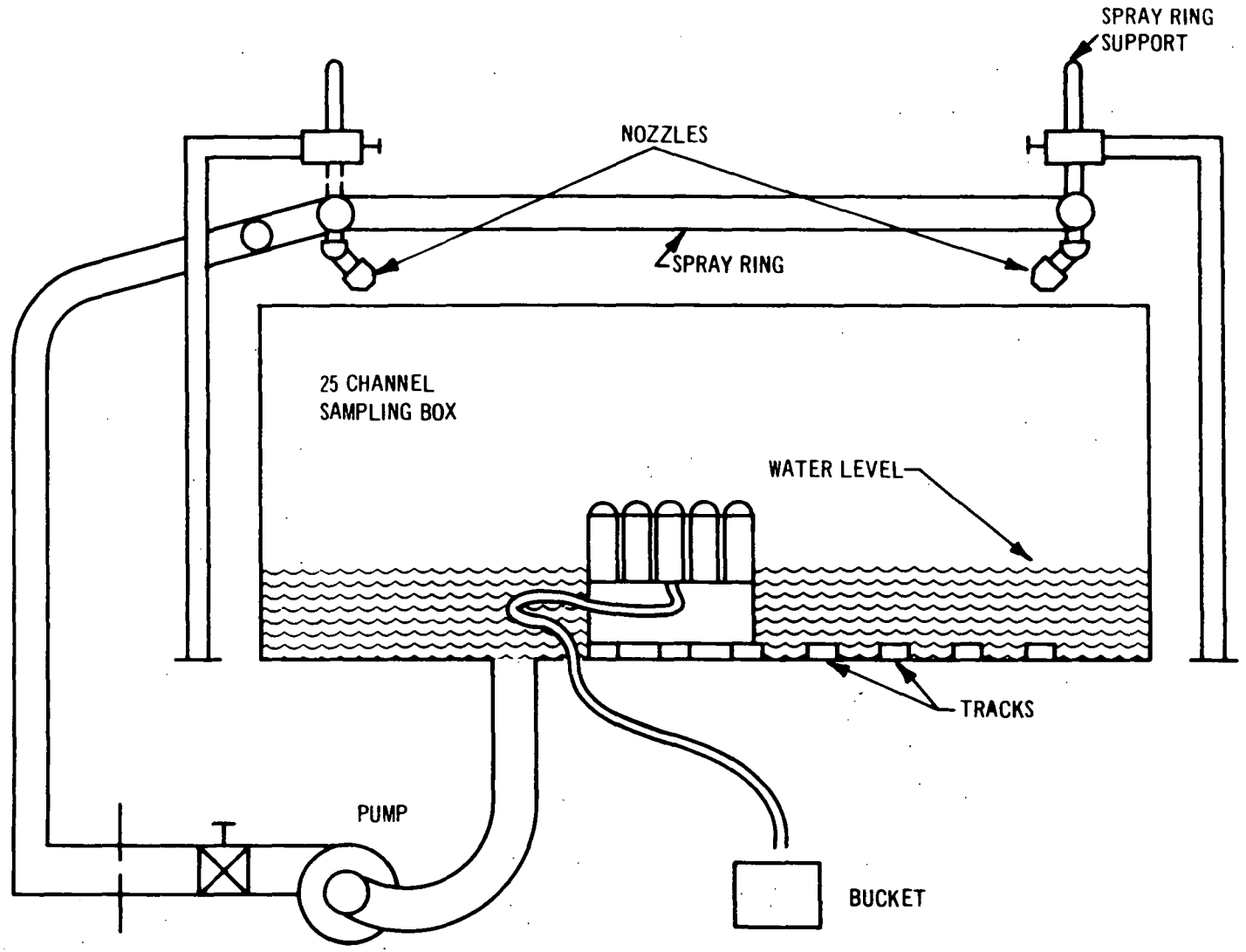


Figure II-C-1 - Schematic of Rod Test Setup

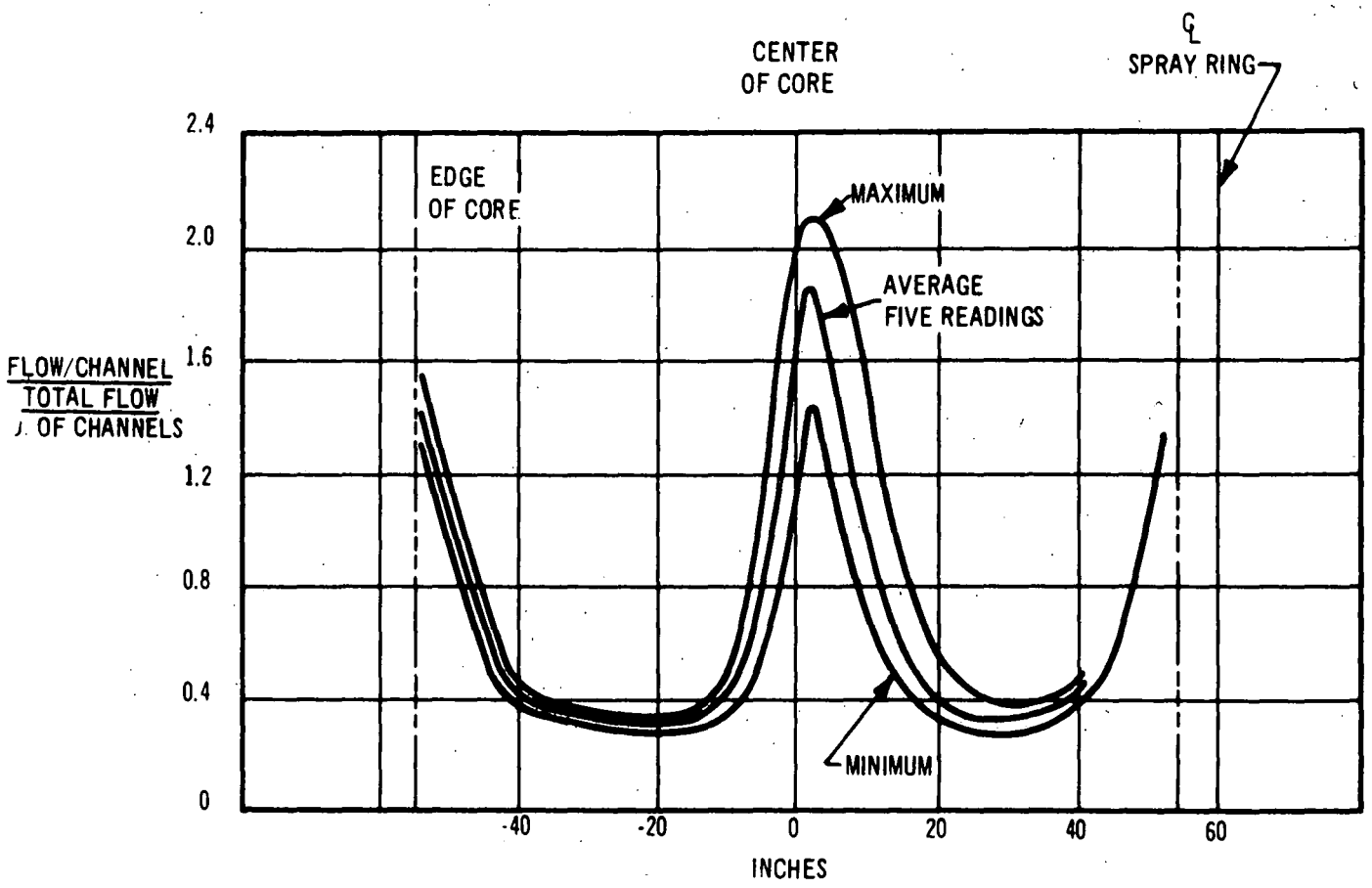


Figure II-C-2 - Typical Results with "Full-Jet" Nozzles

A TO H INSTRUMENTED RODS

● 1.15 POWER

●● 1.30 POWER

CHANNEL TC

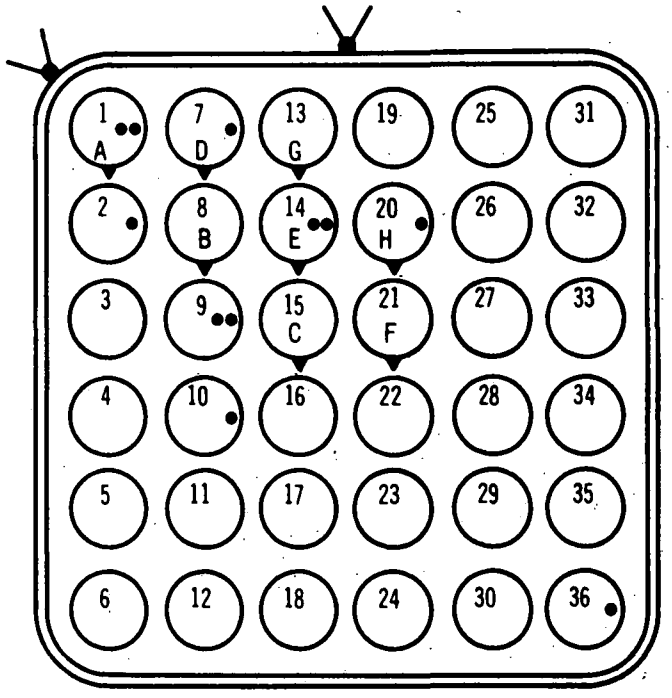


Figure II-C-3 - 36-Rod Instrumentation

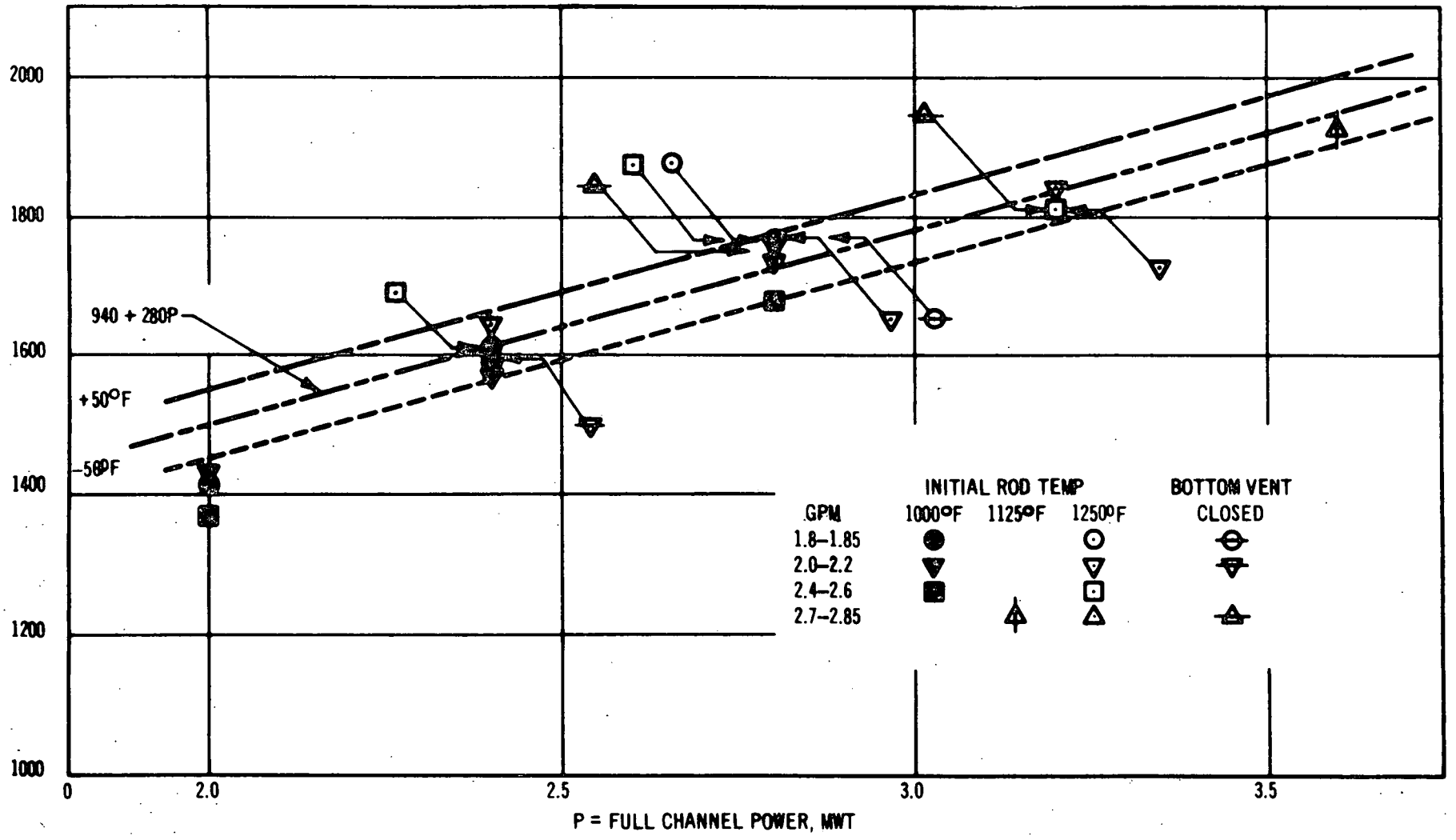


Figure II-C-4 - Highest Rod Peak Temperatures During Emergency Spray Cooling Transient

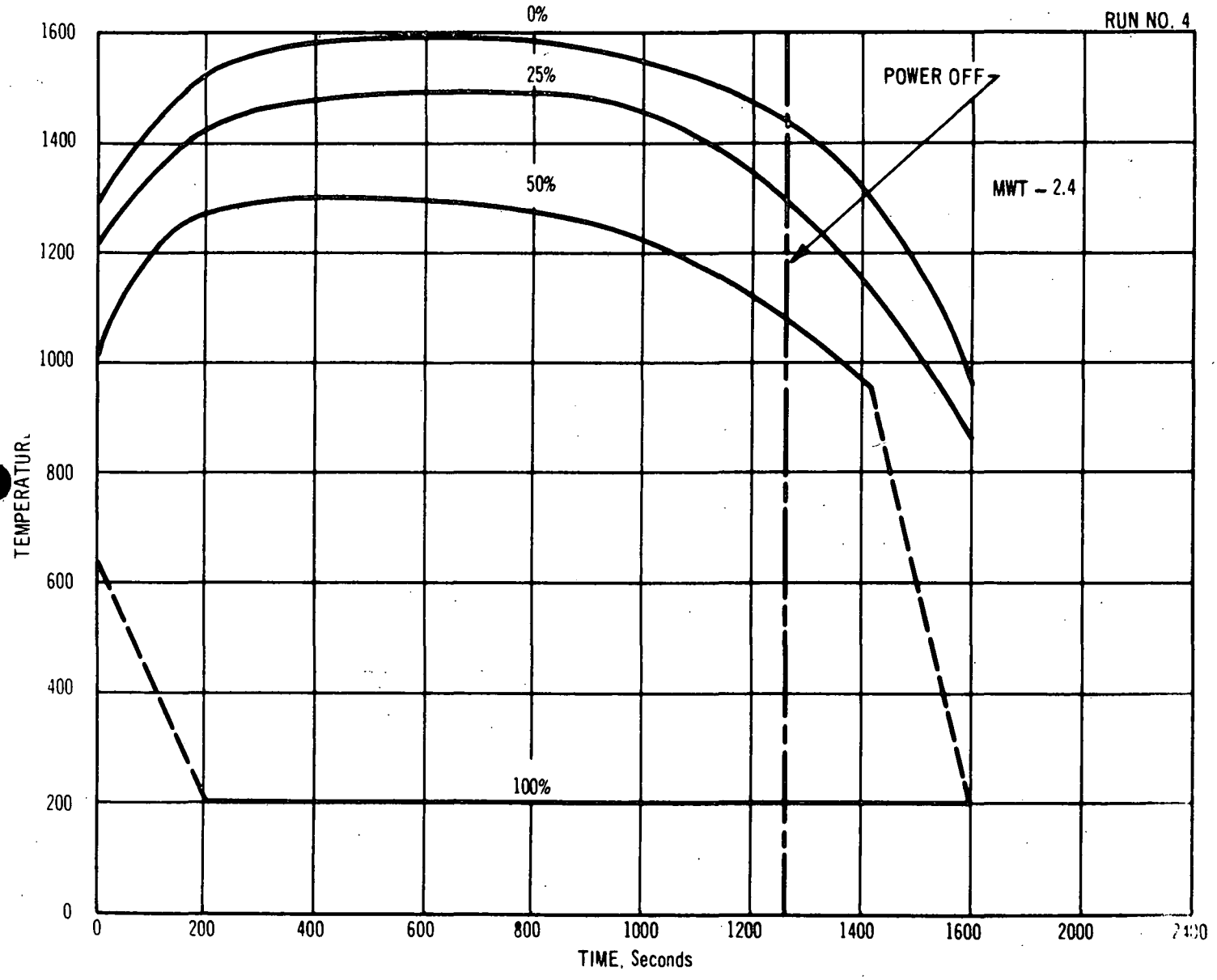


Figure II-C-5 Spray Water Temperature Versus Time for a Given Bundle

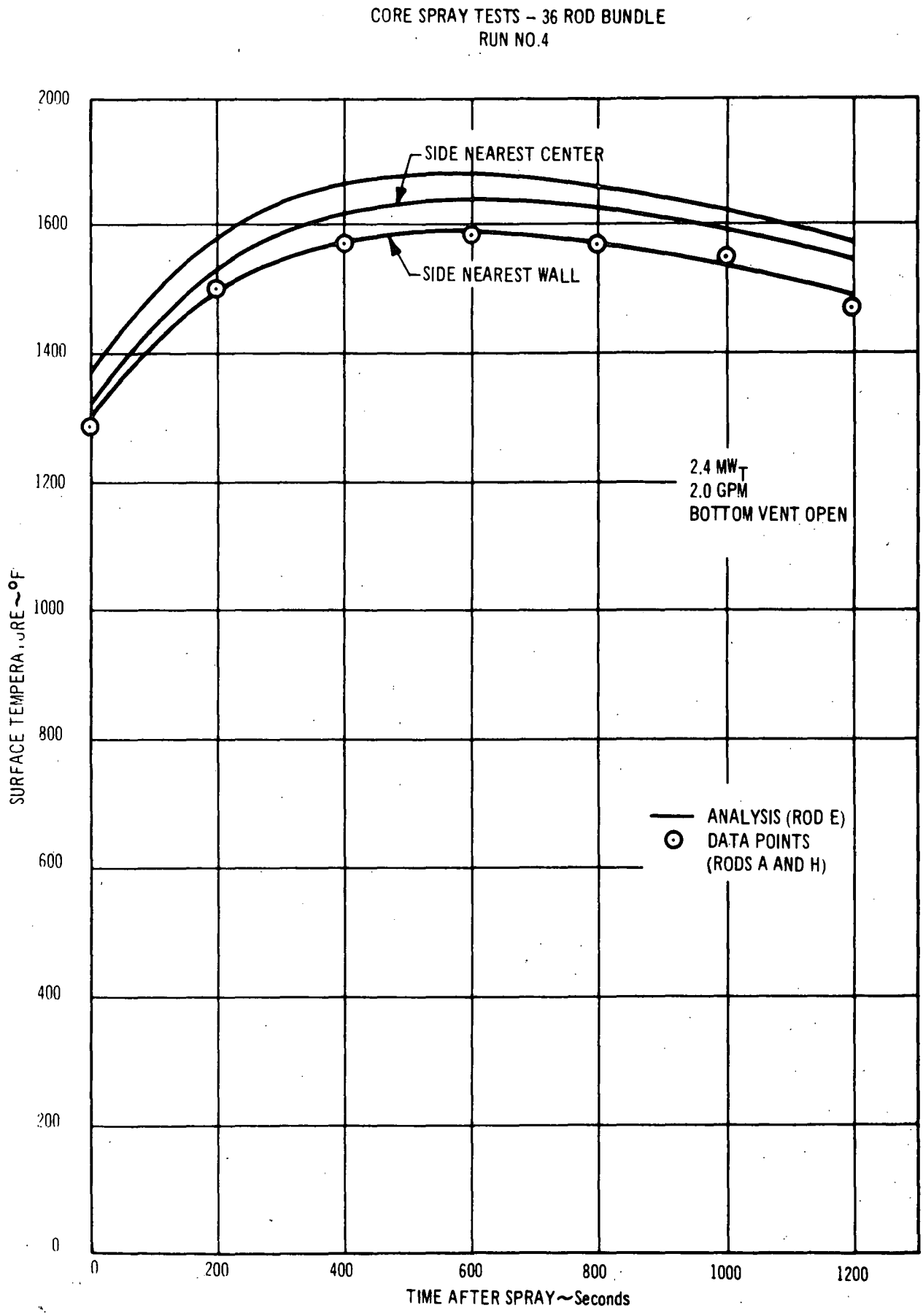


Figure II-C-6 - "Time at Temperature" Curves

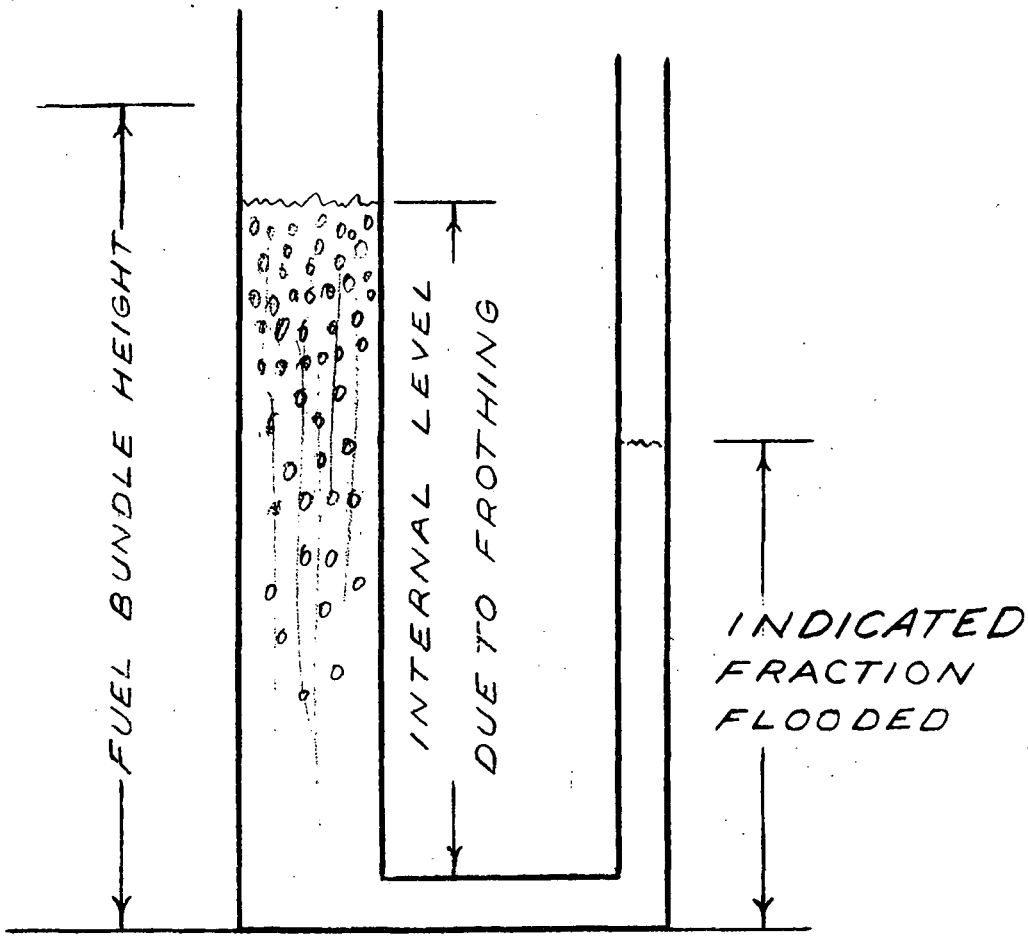


Figure II-C-7

FUEL BUNDLE FLOODING TEST

APPENDIX II-D

Heat Transfer During Blowdown

1. Introduction

Experimental testing was done at the General Electric Atomic Power Equipment Department during the summer of 1965 to determine the transient heat transfer coefficients existing during blowdown of a steam water system at 1000 psia. The results of the investigation indicate that two definite regimes of heat transfer exist: 1) nucleate boiling heat transfer continues for a significant time after the initiation of the accident and 2) convection heat transfer deteriorates as a function of time. The results of these experiments, namely heat transfer coefficients during blowdown, have been made available for application to boiling water reactor hazards evaluation. Bascially, the heat transfer existing during the blowdown determines the amount of effective core cooling and the subsequent temperatures of the fuel rod cladding prior to the activation of the core spray system.

It is concluded from the experimental tests that film blanketing of the fuel rods is delayed in the blowdown of a high pressure boiling water system. The high boiling heat transfer coefficients exist until the water film on the fuel rods has dried out, then the heat transfer coefficient decreases inversely proportional to time. The duration of the nucleate boiling regime, time to dry-out, was found to depend primarily on the power applied to the test section. The minimum dry-out time for each run is plotted as a function of fuel rod

surface heat flux on Figure II-D-1. It was found that all of the runs are correlated by the same line regardless of the break type (i.e. top, bottom or double ended break) or the initial conditions in the test section. A theoretical dry-out time was calculated on the basis of vaporizing the water in the core at the time of the rupture. The empirical dry-out time determined from these tests are about 30 percent of this theoretical time, indicating that much of the water initially in the test section is carried out with the expanding steam.

The experimentally determined heat transfer coefficients after the dry-out time were normalized to the boiling regime coefficient* (e.g. h/h_B) and the time after dry-out was normalized with the dry-out time (e.g. $\Delta\Theta/\Theta$). The lowest heat transfer coefficient in the heated section is shown as a function of the time after dry-out in Figure II-D-2. The amount of scatter shown in Figure II-D-2 is not surprising in view of the phenomena being correlated and is not excessive for the intended application of the data. Here again, all of the runs are correlated by the same line and no significant influence is observed due to the initial conditions for the test or the heat flux.

2. Method of Solution

In order to experimentally determine the heat transfer coefficients which are applicable for hazards evaluation analysis, it is necessary that the blowdown test system be representative of a large boiling water reactor. A typical BWR contains about 14,000 ft^3 of steam and water at 1000 psi, and blowdown would be due to rupture of a 24 inch recirculation pipe. The blowdown rate will de-

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* The nucleate boiling heat transfer coefficients as determined during the dry-out time were found to agree quite well with frequently used correlations for steady state boiling such as the equation proposed by Jens and Lottes (1).

pend primarily on the total mass of the water contained in the system and on the size of the break. Core cooling will depend on how this water is distributed in the system with respect to the core, the break, and the major flow restrictions with the system. As shown in Figure II-D-3, the core contains a very small amount of the water. Most of the water is located above the core in the upper plenum, and a large share is located in the plenum at the core inlet. The significant flow restrictions in the system are the core inlet orifices and the steam separators.

The criteria for the test is that the apparatus must have the same distribution of water inventory as the reactor system, and the flow restrictions above and below the heated section must produce pressure drops characteristic of normal reactor operation. The break area is then sized for the test so that it is proportional to the reactor break area in the same ratio as the water contained in the test is to the water contained in the reactor. The blowdown rate of the test system will then be the same as would be experienced by the reactor, and the transient heat transfer coefficients during blowdown can be determined by direct measurement in the test system.

3. Experimental Program

The blowdown test system is shown schematically in Figure II-D-4. A four rod test section in a square flow channel ($A=.00743\ ft^2$) is used to simulate the reactor core, and the required mass distribution in the test system is provided by adding tanks to the system at the inlet and outlet of the test section. These tanks are sized in proportion to the volume of the heated section. The rods are made from 7/16 inch diameter inconel X tubing with 0.022 inch wall thickness. The internal

restrictions, necessary to simulate the core inlet orifices and the steam separators, are provided by orifices placed above and below the heated section. Power is produced in the rods by resistance heating in the wall over a four foot length. Current is brought into the rods by copper electrodes silver soldered to the ends of the tubes.

The blowdown transient is initiated by rupture of a disk in a vent line; tests are run for a top break, a bottom break, and a combined top and bottom break. At the start of the transient, control rod scram valves isolate the system to prevent the entire heat transfer loop from blowing down through the test. The rods are instrumented with thermocouples to measure the wall temperature at three axial positions, and at the locations shown in Figure II-D-4. The thermocouples (8 millisecond time constant) are spot welded to the inside surfaces of the heater tubes.

4. Experimental Heat Transfer Results

The pressures, power, and temperatures are recorded with oscillographs during the blowdown transient. Typical traces are shown in Figures II-D-5 through II-D-7 for a top break, bottom break and a double ended break. The system pressure decay recorded in these tests is found to be very close to that predicted for a large scale reactor system, particularly during the initial period when the core is being cooled. This agreement confirms the scaling used in these tests.

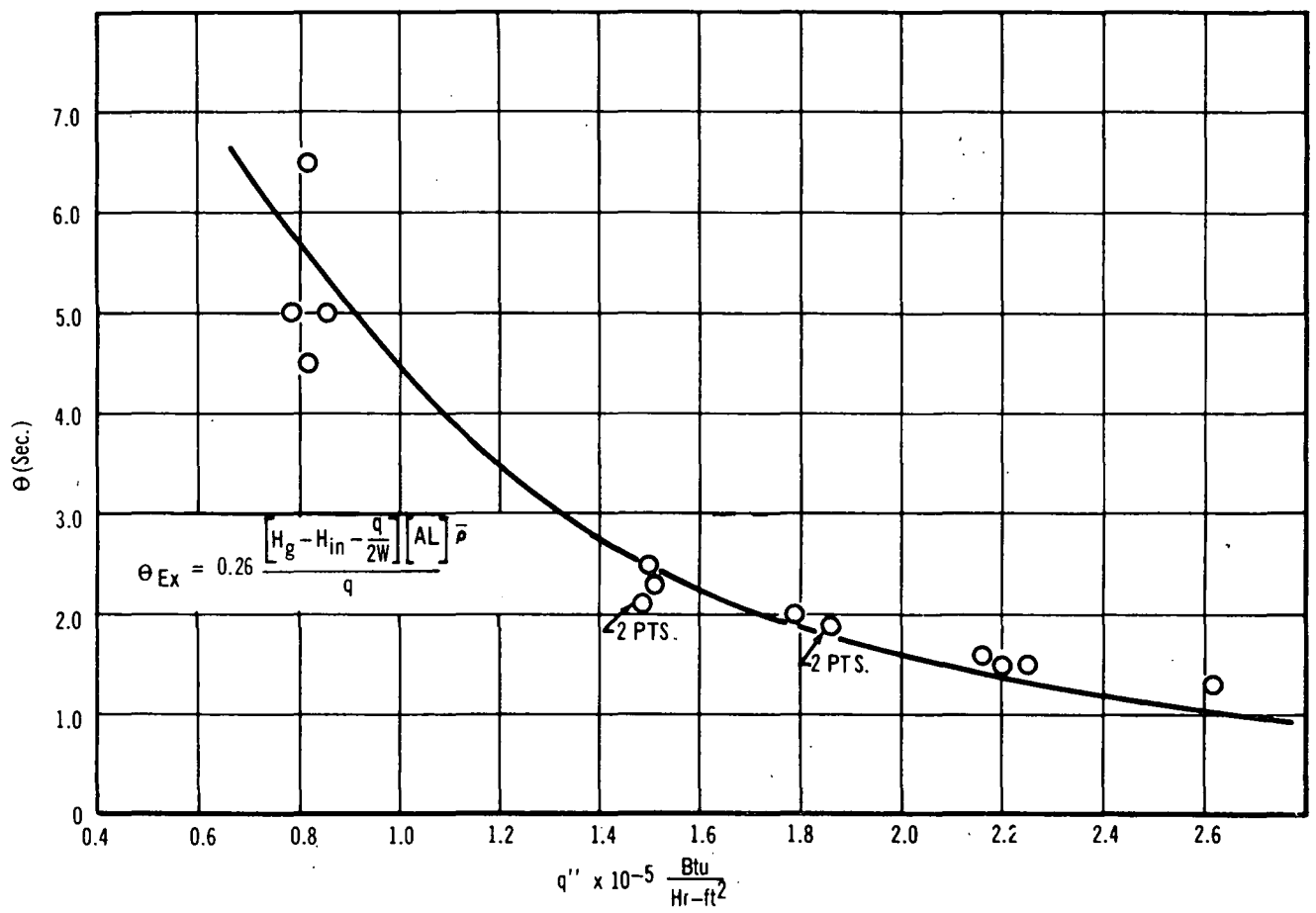
Power to the rods is held constant during the initial period of the blowdown transient. The traces show that the rod surface temperatures hold steady also (cf. Figures II-D-5 through II-D-7) which demonstrates that there is no immediate steam blanketing but rather that nucleate boiling persists after the rupture. The

rods continue to be cooled by nucleate boiling until the water film on the rods has dried out, at which time the wall temperature starts to climb.* Heat transfer coefficients are determined for the nucleate boiling regime on the basis of the measured wall temperature and the saturation temperature corresponding to the measured system pressure.

Wall temperatures are recorded simultaneously at 13 locations in the heated section, and the dry-out time is taken as the time from the rupture until the first surface temperature starts to climb. All core locations do not dry out simultaneously, as may be seen in Figure II-D-6. The duration of the nucleate boiling regime is found to depend primarily on the power applied to the test section.

When the water film on the rods has dried out, the wall temperature climbs due to the deteriorating heat transfer coefficient. To prevent damage to the test section, the power is tripped when the wall temperature reaches 1000°F. After the rods have cooled the power is turned on again, and the immediate rise of wall temperature indicates that not enough water remains in the test section to re-wet the rods. If the surface was completely insulated by steam blanketing when the water film has dried out, the surface temperature would climb linearly at a rate depending on the heat capacity of the heater wall. It is found in these tests, however, that the rate of wall temperature rise is somewhat less than that for a completely insulated surface, which indicates that some heat is transferred to the steam during this period. The heat transfer coefficient is defined on the same basis as in the nucleate boiling regime, and is found to decrease inversely proportional to time after dry-out.

* This delay before the wall temperature climbs cannot be explained by the response time of the instrumentation because the delay time is on the order of a few seconds, and the time constant of the heater wall and thermocouple is only about 8 milliseconds.



Where: Θ , Dry-out time, seconds

Θ_{EX}, experimental, seconds

H_g , enthalpy of steam, Btu/#

H_{in}, core inlet enthalpy, Bty/#

q , heat addition, Btu/hr

q'' , surface heat flux, Bty/hr-ft^2

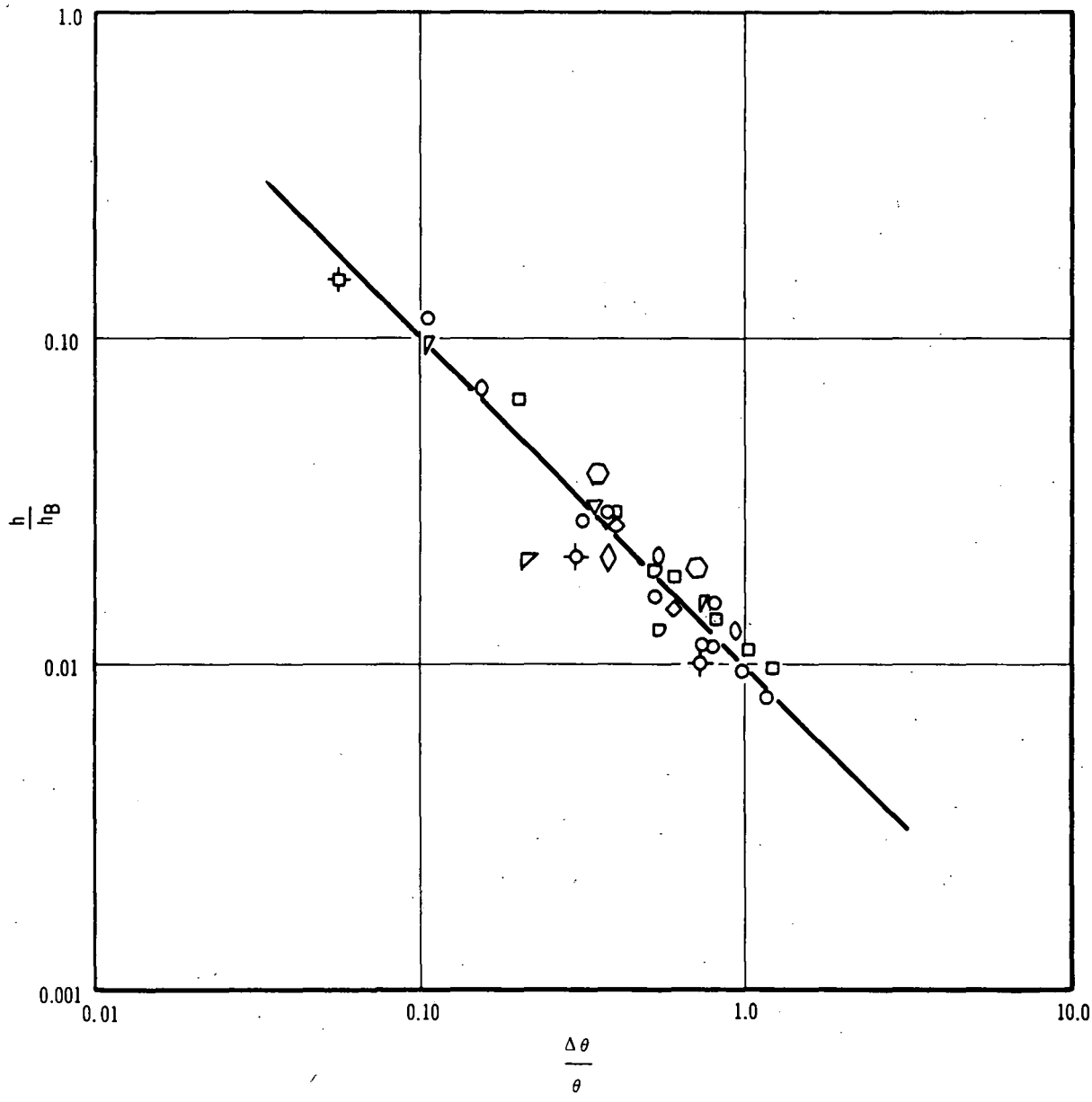
W , flow rate, #/hr

$\bar{\rho}$, two phase density, #/ft^3

A , heat transfer area, ft^2

L , length of rods, ft

Figure II-D-1 Minimum Duration of Nucleate Boiling Region During Blowdown from 1000 psi



where: h , heat transfer coefficient after accident, B/hr-ft^2°F

h_B, nucleate boiling heat transfer coefficient, B/hr-ft^2°F

Θ , dryout time, seconds

ΔΘ, time after dryout, seconds

Figure II-D-2 Minimum Heat Transfer After Dry-Out During Blowdown from 1000 psi

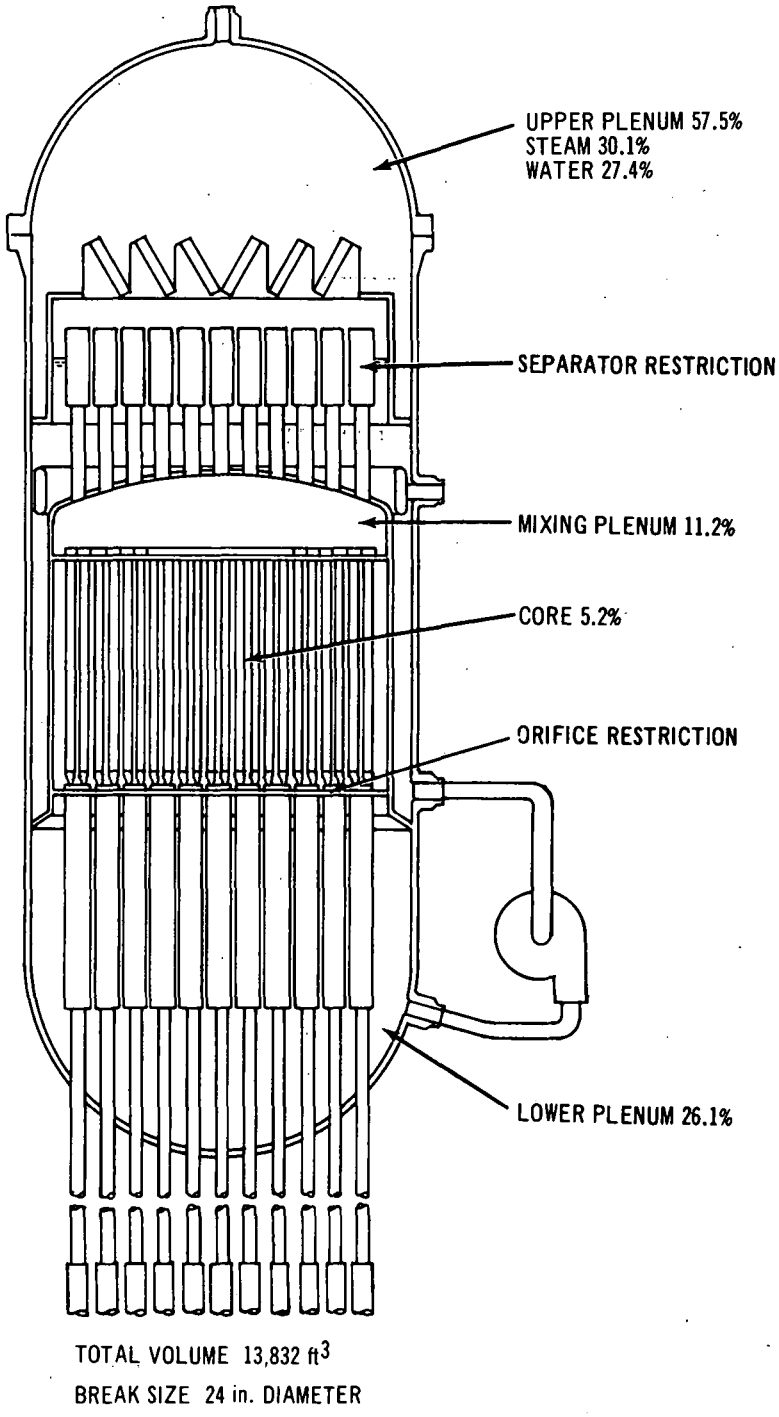


Figure II-D-3 Distribution of Water and Steam in a Large Boiling Water Reactor

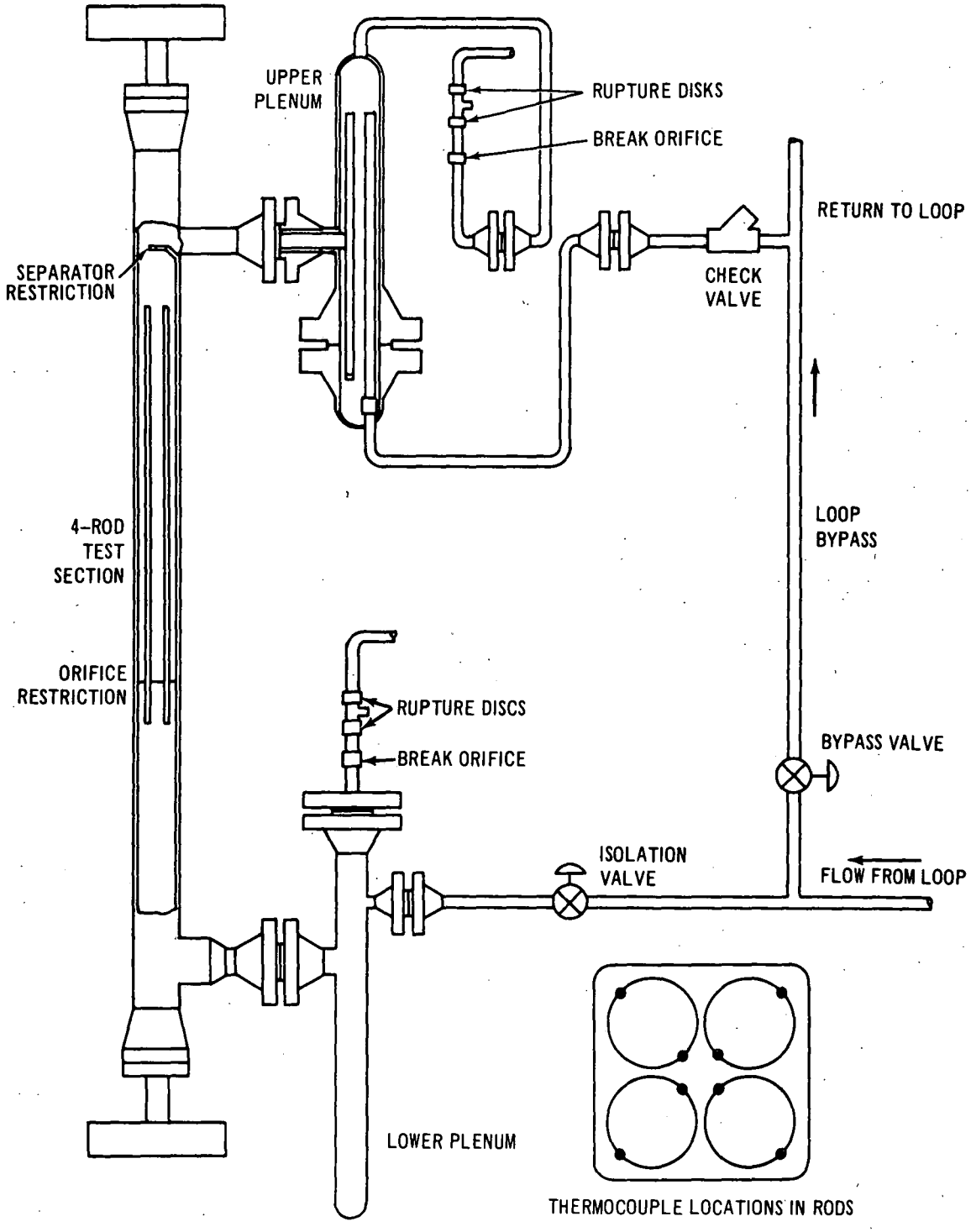


Figure II-D-4 Schematic of the Four Rod Test System

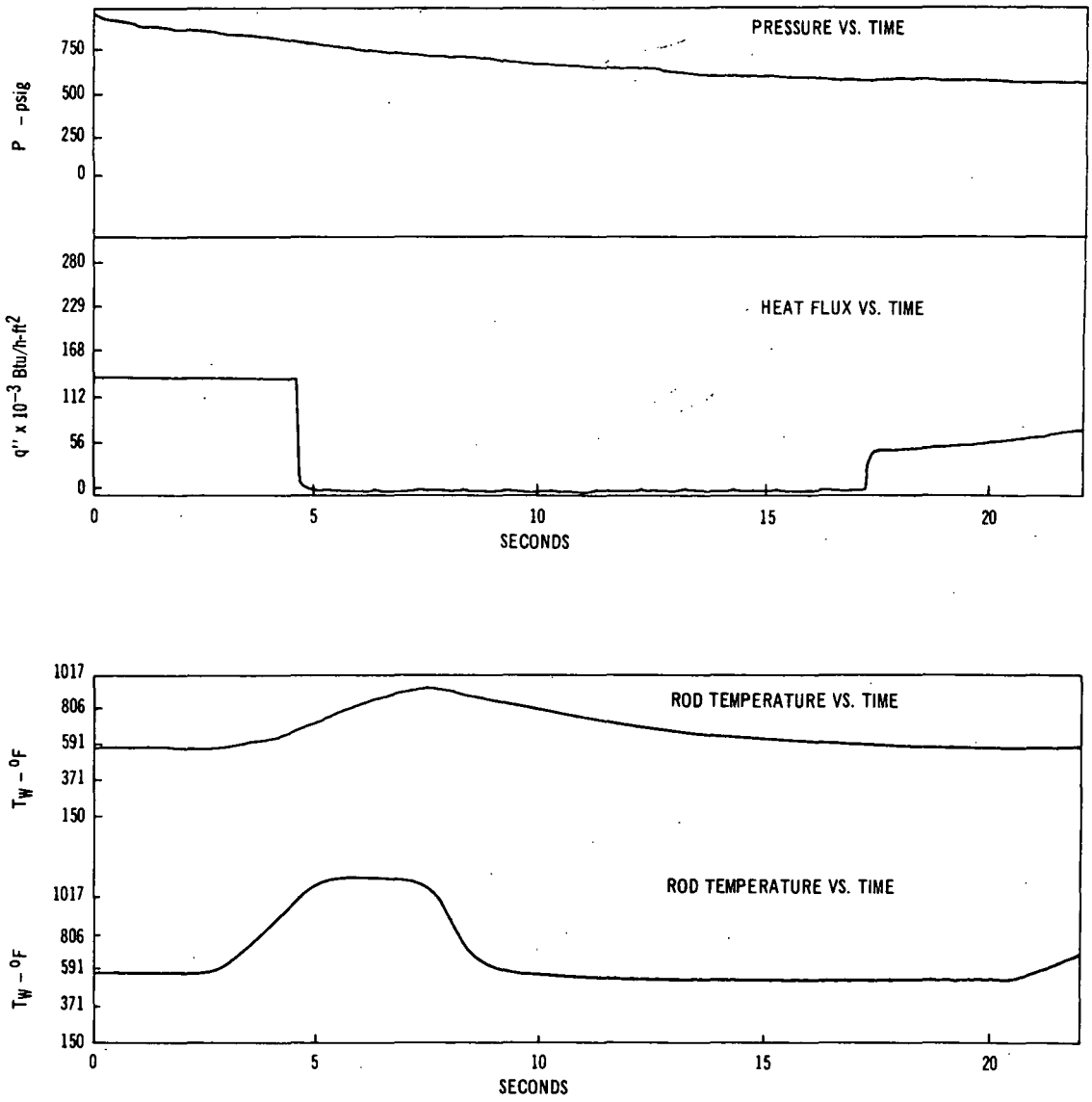


Figure II-D-5

Transient Recordings Made During Blowdown - Top Break, Run Number 16

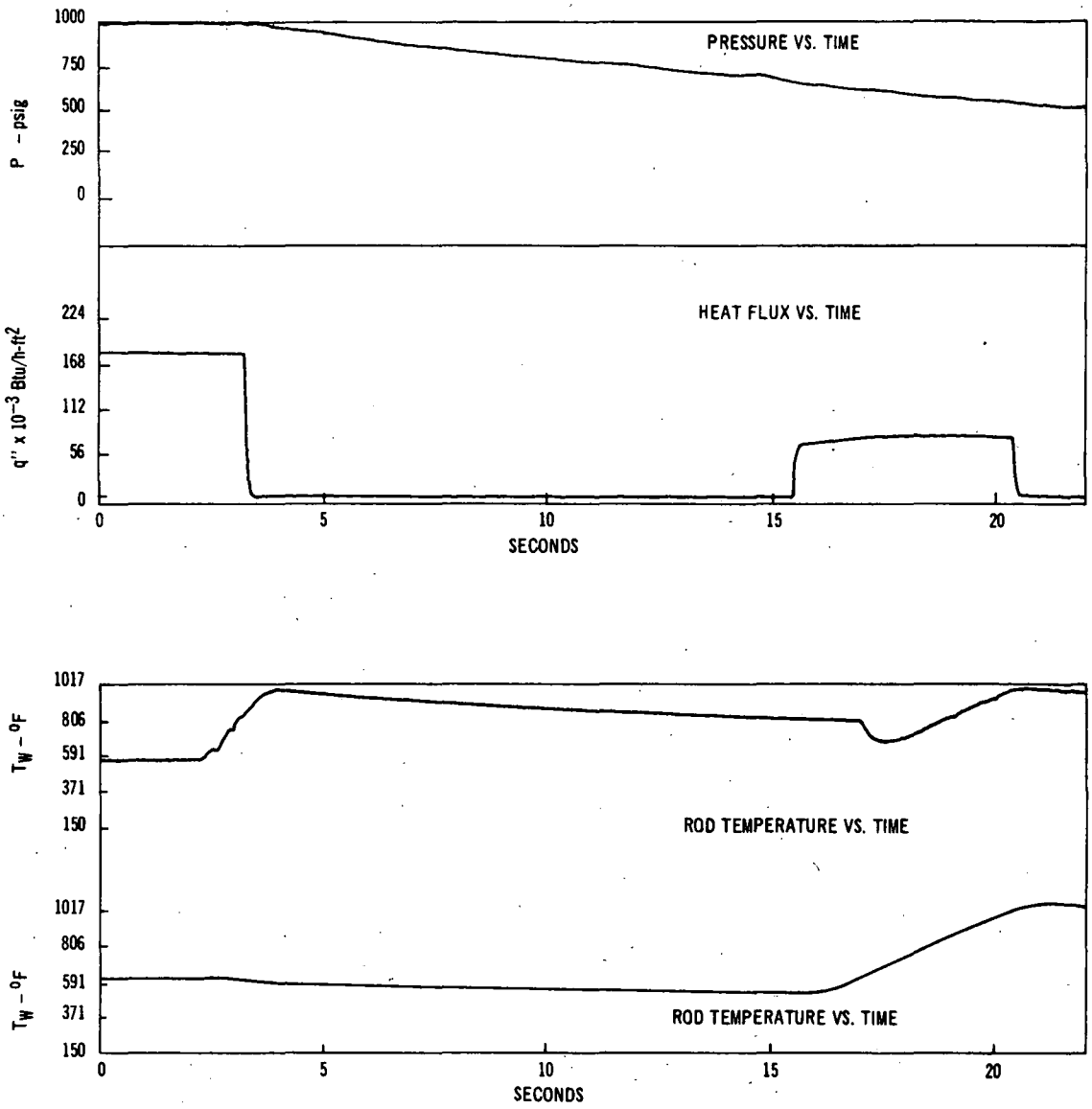


Figure II-D-6

Transient Recordings Made During Blowdown - Bottom Break, Run Number 12

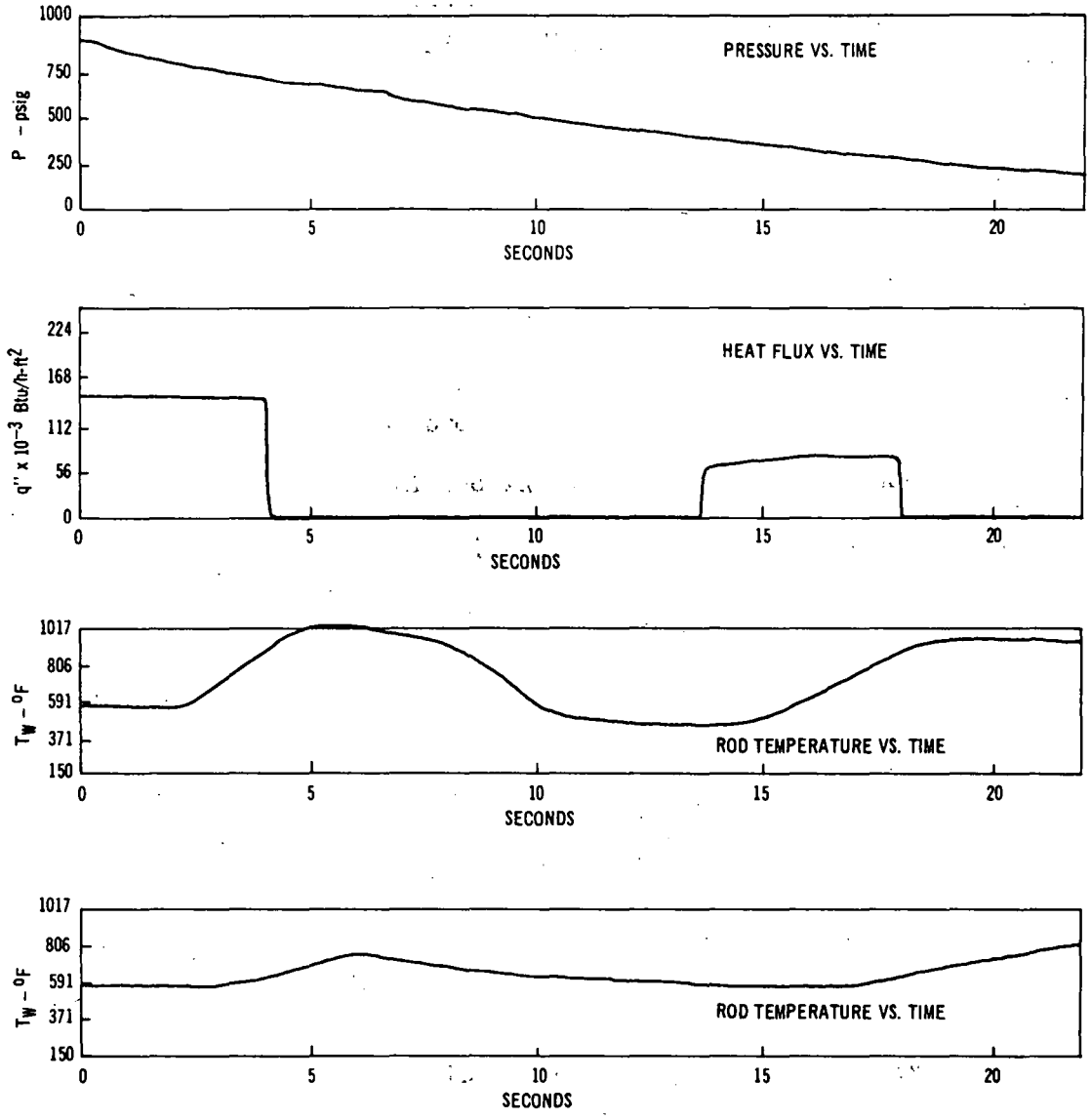


Figure II-D-7

Transient Recording Made During Blowdown - Double End Break, Run Number 20

II.3.2 A PREDICTION OF CORE SPRAY SYSTEM AVAILABILITY

MODEL

The core spray system consists of two separate loops. Each loop has full capacity redundant active components, that is, any component which must move in order to contribute to system success is duplicated. For this reason, each loop has redundant pumps operating in parallel, two motor operated normally closed valves in parallel, and two check valves in parallel. The other motor operated valves are not duplicated because their usual position (open or closed) is the same as the position that will contribute to overall system success.

A block diagram availability model of the core spray system is shown in Fig. II.3.2-1. In order for the core spray system to be successful, it must be possible to trace one (or more) continuous path from left to right without encountering a failed element. The only common point between the two loops are the triplicated strainers feeding the common header drawing water from the suppression pool. Otherwise, there are always two paths, and in places there are four paths through the matrix.

The model is to assist in predicting the availability of the core spray system based on what is known of the failure rates of the individual components from which it is made. The results of such an analysis are meaningful only to the extent that the model actually represents the system in all its environments, and to the extent that the failure rate data is truly representative of the population of parts from which the system is made. Even if the model and the data were perfect, the result is phrased in probabilistic terminology which implies that experimental verification on a system such as core spray is never possible. Nevertheless, the results are of considerable use in drawing comparisons between alternative designs using approximately the same components.

COMPONENT FAILURE RATES

Component failure rate data is reported in the literature.[1, 2, 3, 4, 5]

References:

1. Reliability Engineering Data Series Failure Rates, April 1962 by D. R. Earles and M. F. Eddins, AVCO Corporation.
2. Report on Reliability of Electric Equipment in Industrial Plants, July 1962, by W. H. Dickinson, AIEE.
3. Nuclear Engineering, April 1963, page 125.
4. MIL - HDBK - 217
5. Failure Rate Data Handbook, Bureau of Naval Weapons, U.S. Naval Ordnance Laboratory, Corona, California.

In general, it is not complete and it is difficult to be sure that the data reported is representative of the equipment proposed for use in the core spray system or that the environments are similar. However, the total spread in data is not great, and knowledgeable people with field experience have been able to confirm that the relative ranking of component failure rates is approximately correct. Some of the data must be adjusted to fit the system under consideration using best engineering judgment as to the conditions prevailing.

Triple Strainers---It is believed that the strainer plugging is not an influential factor in evaluating the availability of the system. The strainers are located in a quiesent portion of the pool. They are sized so that one strainer could carry full flow to one core spray loop and one containment spray loop with a pressure drop which is negligible compared to the NPSH of the pumps. The approach velocity is held so low that in all probability most extraneous materials that would tend to plug the strainers would either settle to the bottom or float to the top and not be drawn into the system. The strainers are off the botton, further reducing the probability of plugging from floating or sinking debris. There is a pressure indicator on the suction side of the pump, so that a plugged strainer would show up on test.

During normal operation there would be very little tendency for the strainers to plug. During an accident involving pressure suppression, it might be possible for loose material to be blown with the rush of steam into the suppression pool and aggravate the plugging situation. Every effort is made to avoid using materials in the drywell that would contribute to this type of plugging. In addition, the three strainers are dispersed around the periphery of the torus in such a way that they are less likely to encounter the same phenomina.

Valves---A motor operated valve which must stroke has a failure rate of 5.65 failures per million hours. This includes the valve, motor, gear box, and motor starter. This failure rate also assumes frequest testing and thorough and regular periodic preventative maintenance.

If the motor operated valve does not have a stroke, it is assumed to have a failure rate of only one tenth, or 0.57 failures per million hours. Failure in this case is to have the valve close when it should be open and vice versa. Where possible, the wiring is so arranged that two independent electrical faults would be required to cause this type of failure.

Check valves have a failure rate of 0.1 failures per million hours when operated at room temperature, and 1.0 failures per million hours when operated at high pressure and temperature.

A failure of a locked open valve is probably a human failure; that is, a valve which is supposed to be open is in fact locked closed. Subjectively, one would not expect this to be a frequent occurrance, yet there are some evidences to indicate that it has happened. The valve is located at a point in the system which would tend to make operation of the valve rather infrequent, probably of the order of once per year when the pump is undergoing inspection. Ordinarily, procedure would call for testing the system for operation after an inspection and such a test should immediately reveal an erroneously closed valve.

In a task of this nature one should be able to expect a rather high level of performance from a plant operator. If he was so unreliable as to make one mistake out of every 10 assignments, his poor performance would be detected almost immediately and he would be retrained or dismissed. A good plant operator would make relatively few mistakes. Even so, it would be difficent to prove a rate of less than one mistake is 1000 assignments. Considering that two almost independent mistakes are necessary in order that a valve be locked closed and left closed after test, allowance for one mistake is 1000 seems conservative and defendable.

If the valve is operated once per year and left closed instead of open once in 1000 times, it can be assumed to have a failure rate of approximately 0.1 per million hours. This is at best an estimate, but it is doubtful that this failure rate is significant compared to the pump failure rate and thus has little bearing on the analysis outcome.

Pump---In the model, the pump block includes the pump, motor, and the motor starter. The failure rate for this block is assumed to be 7.15 per million hours. This number is derived from numbers reported in the references.

The largest uncertainty here is in the duty cycle. This application is standby, whereas most pumping applications require almost continuous operation. Although wearout is probably not as likely on standby equipment, there may be other deteriorating influences that are just as bad. Also, certain failure modes that would be considered true failures in continuous operation could be ignored in an emergency. For example, a leaky seal on a pump would probably not be considered a failure if the pump could continue to deliver full flow for the duration of the emergency.

Sparger---The spray sparger could fail by plugging or shearing a substantial number of nozzles. The sparger is tested for flow each shutdown, so random failures will not be allowed to accumulate. Actually, many simultaneous failures would have to occur to render the spray ineffective. It is believed that

this type of failure is negligible compared to other failures in the loop.

The sparger is subject to other failures that are not random. If it is postulated that core spray is required because there has been an accident, then it is of concern to consider what effect the accident had on the integrity of the sparger. Actually, such damage is not limited to the sparger but may include all points within the loop where single point damage disables the whole loop. In order to deal with this contingency in the analysis, assume that in the event of an accident requiring core spray, the sparger will survive the accident undegraded with a probability "P".

Analysis of Model

Utilizing the methods of Appendix I, the availability model of Fig. II.3.2-1 may be reduced to the much simplified model of Fig. II.3.2-2. One core spray loop is represented by blocks A and B. Block A represents the probability that the required combinations of pumps and valves in one core spray loop will be available when an accident requiring core spray occurs, and block B represents the probability that the accident itself will not impair the operation of the loop. Blocks C and D represent the second loop.

Using failure rate data, we are able to generate a numerical probability for blocks A and C. This number is at present only a prediction. However, it is a number that can be confirmed (or refuted) by periodic testing of the system to verify its readiness. Blocks B and D have only the notation "P" which stands for the probability that the accident which initiates the need for core spray does not degrade the system. In this area there is no data to guide and no intent to field test under actual accident conditions.

If "P" is assumed to be 1.0, that is the accident cannot in any way degrade the core spray system, then the probability of having a successful core spray operation is

$$\text{Prob Success} = 0.9999984 \quad (P = 1.0) \qquad (1)$$

Stated another way, if there are 10 million events where core spray is required, we could expect failure 16 times.

If "P" is not 1.0, the relationship is

$$\text{Prob Success} = 1 - (1-099875P)^2 \quad (0<P<1.0) \qquad (2)$$

The above relationship assumes that the failures of the spargers are independent events. Actually, they are dependent on the magnitude and location of the accident that forced the use of core spray. These relationships are complicated and unexplored.

If the accident is outside the reactor vessel, the two core spray systems are so arranged that a single accident is not likely to encompass both systems. If the accident is inside the vessel and one sparger is thus damaged, there is a finite probability that the other sparger could suffer damage also.

Returning to the assumption of independence, the best relationship that bears of simple mathematical treatment is to assume that if the probability of failure of one sparger is high, the probability of failure of the other is also. In other words, assume that "P" is the same for both blocks B and D as was done in equation (2).

A plot of equation (2) is shown in Figure II.3.2-3 for convenience in plotting, the probability of system failure is used in place of the probability of system success. Likewise, (1-P) is used instead of P. Figure II.3.2-3 shows that for low probabilities of sparger failure, the probability of core spray system failure is also very low and is limited only by the components and not the sparger. Notice from Figure II.3.2-3, that if the probability of sparger failure is less than 10^{-3} or .001, the influence of sparger failure on the probability of core spray failure is negligible.

OTHER MODELS

In the process of design, models for several variations on the core spray system were evaluated. One that is of particular interest is one utilizing three 50% pumps in each loop. If system success is defined as being able to achieve 100% flow in either (or both) loop(s), the system has almost as high availability as the system models in Figure 1. If system success could be defined as 100% system flow including the possibility of 50% flow in each loop, the availability for the three 50% pump case model would be slightly higher than the two 100% pump model. Such a definition of success if plausible because a single 50% pump will give adequate coverage with about 70% rated flow capability for a total rating of about 140% for the system operated in this mode. The 50% pumps have an advantage in being able to start from the emergency diesel generator.

If two pumps are connected in parallel, check valves are utilized to prevent recycling the water through the idle pump. In the case of the check valve failing stuck open, an analysis of this failure mo e was made and found to be negligible when using the listed failure rates for pumps and valves. A sensitivity analysis was carried out to see if the outcome is critically dependent on the failure rate for the check valve.

The results of this sensitivity analysis are shown on Figure II.3.2-4. Only the pumping circuit shown was analyzed. The probability of system failure is plotted against the check valve failure rate. The analysis assumes that the check valve has two failure modes, open and closed. The lower curve considers that the check valve is perfect with regard to its fail open mode, but has the indicated failure mode for failing closed. With these assumptions there should be no hesitation in operating pumps in parallel, because failure of a check in the open position is not possible. The lower curve shows that the probability of system failure increases slightly for higher failure rates of the check valve.

The upper curve assumes an imperfect check valve that has equal failure rates for the fail open and fail closed modes. As expected, the probability of system failure is higher than when the check valve could not fail open. The difference is not significant for plausible failure rates. Even if the failure rate for the check valve was as high as for a pump, the difference in probability of system failure would be less than a factor of two. Thus, it is concluded that there need not be anypenalty in availability associated with the parallel operation of pumps.

OTHER FACTORS INFLUENCING AVAILABILITY

1. Electric Power

The electrical power for the core spray system is provided from the 138 KV switchyard at Dresden 1. This switchyard is fed by five power lines plus the feed from Dresden 1. This arrangement yeilds a high availability of power to operate core spray in Dresden 2 and Dresden 3. There is no known relationship leading one to believe that loss of this power triggers the need for core spray. If the need for core spray and the loss of this power are truly random events, the coincidence is believed to be an extremely rare event.

Even so, an emergency diesel generator is supplied to provide power to cover the case of loss of power simultaneous with the need for core spray.

The core spray pumps and motor operated valves are split between two buses in such a way that in the event of a power loss on one bus, either one of both core spray loops can still be operated at full flow capacity.

Availability studies on emergency electrical power indicate that power is not a limitation on core spray availability.

2. Instrumentation

The instrumentation and control for core spray is designed with a high level of redundancy and so arranged that testing can be carried out frequently and thoroughly on each loop. Instrumentation is not expected to be a limiting factor on core spray availability.

3. Testing

The system is so designed that it can be tested thoroughly from sensor to output. These tests are easy to conduct and it is expected that they will be performed frequently. The availability analysis was based on a test once each two months for the active components, and a flow test of the sparger nozzles at each shutdown.

The tests should be of such a duration that all equipment is allowed to reach equilibrium operating conditions of temperature, flow, and vibration.

CONCLUSIONS BASED ON AVAILABILITY STUDY

The most conservative analysis of the core spray system yields availability estimates so high that one is lead to the conclusion that some other phenomena, not accounted for by the model, will be more influential in reactor safety than those now known and accounted for.

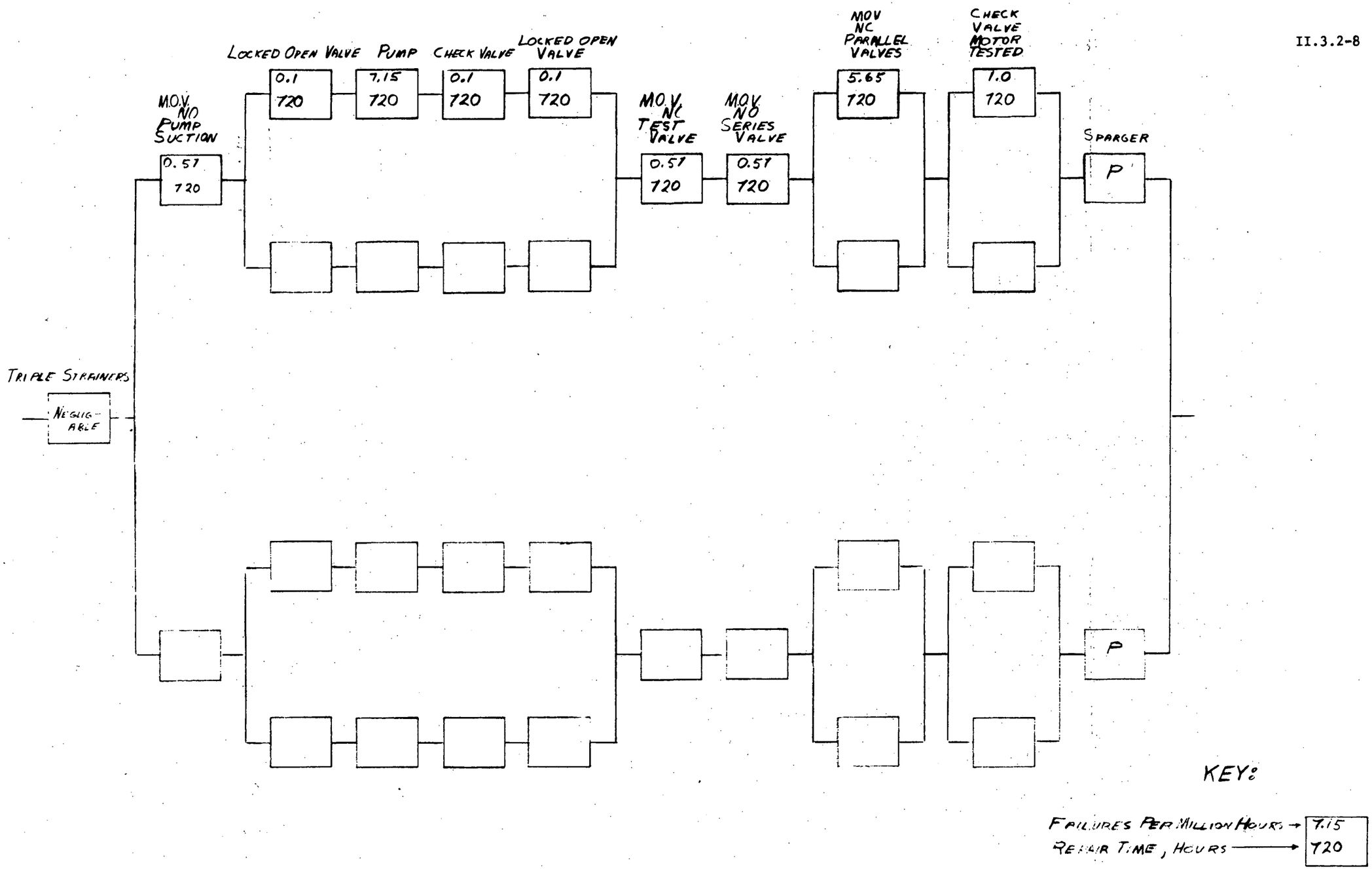


FIGURE II.3.2-1 CORE SPRAY SYSTEM AVAILABILITY MODEL

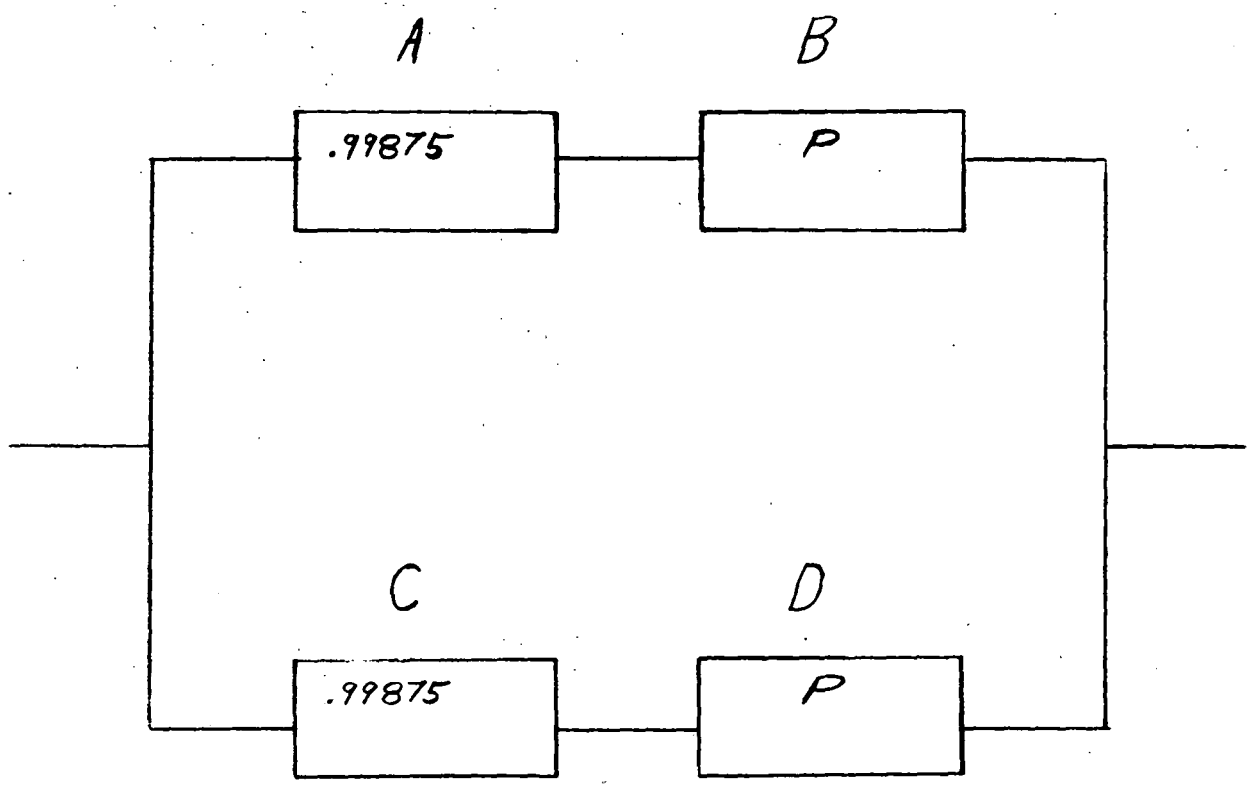


FIG. II.3.2-2 A REDUCTION OF AVAILABILITY MODEL OF FIGURE II.3.2-1

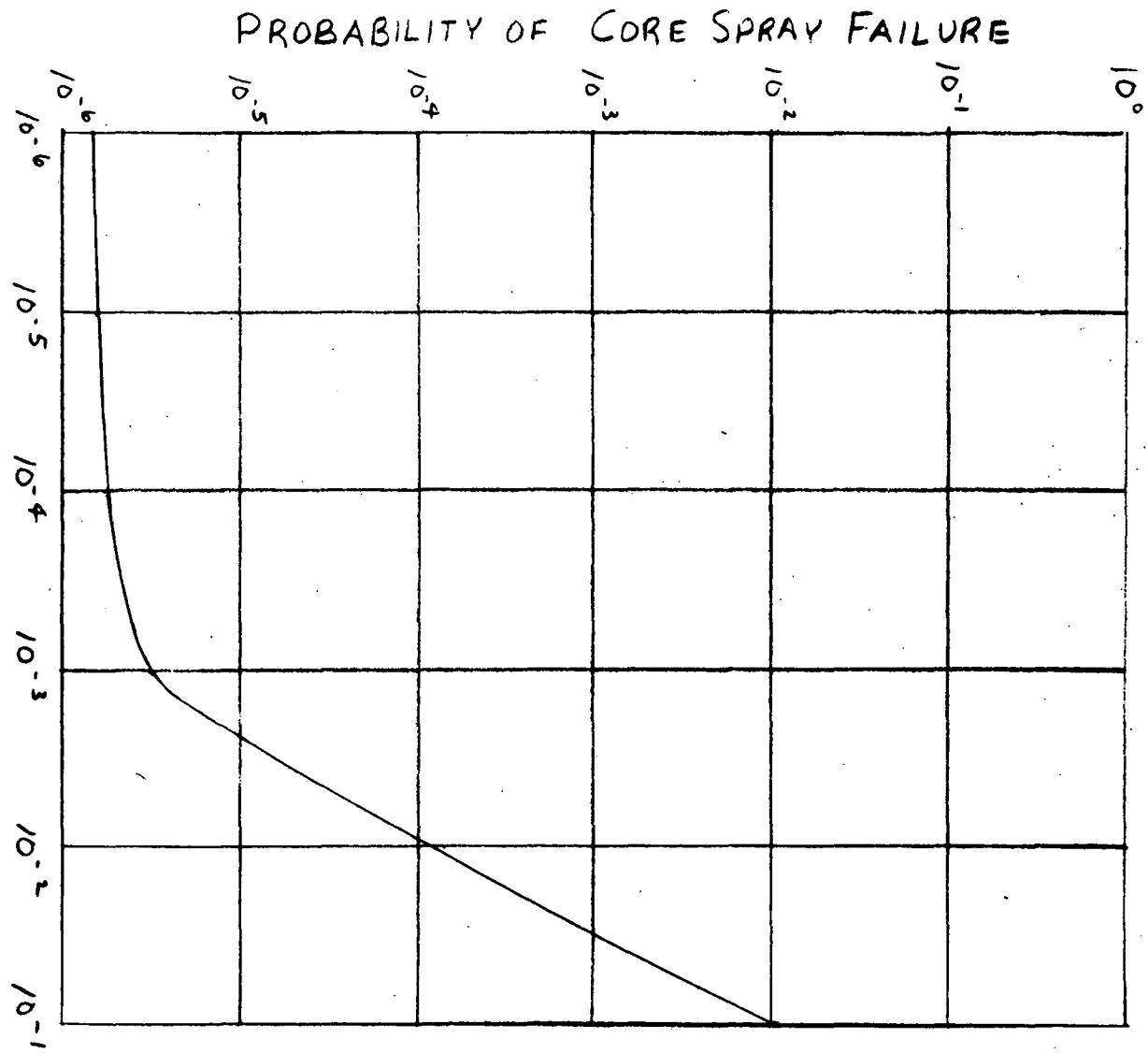


FIG II.3.2-3 A PLOT OF EQUATION 2

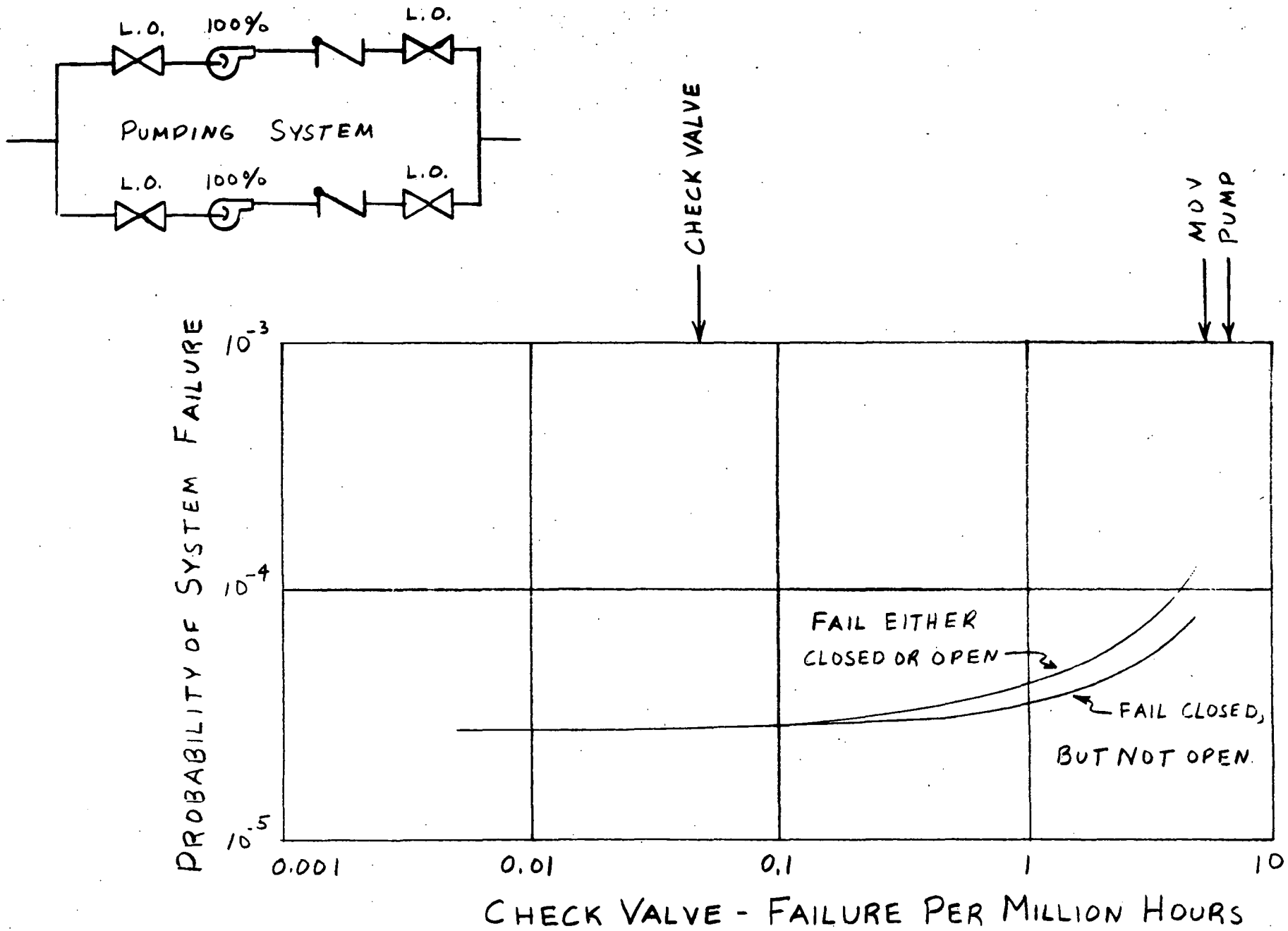


Fig. II.3.2-4. Sensitivity of System Failure to Check Valve Failure Rate

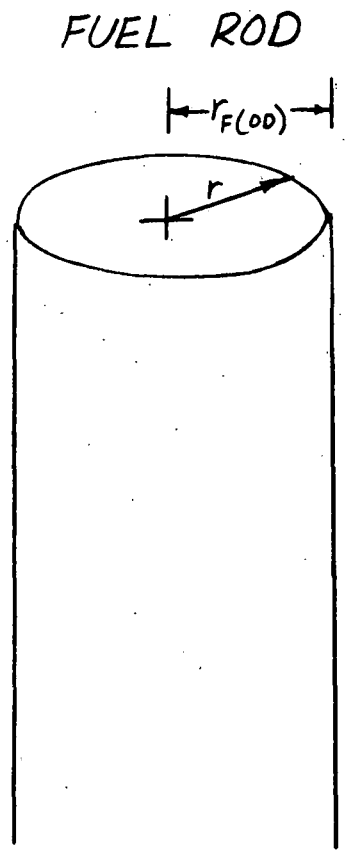
APPENDIX II-E
CORE HEATUP ANALYSIS MODEL

1. Temperature Distribution

The analytic methods used to determine the temperature distribution in the core are described in this appendix. Also included are the methods used to predict metal water reactions.

The temperature distribution in the core as a function of time is described in this analysis. As a starting point the heat transferred between fuel channels is considered negligibly small. Also, the heat transfer in the axial direction is considered negligibly small. Consider now an individual fuel rod. See Figure 1 below. Assuming the properties of the fuel are uniform and the power production in the rod is uniform in the rod, we may write the following energy rate equation for any point in the fuel rod.

FIGURE 1



$$K_F \left[\frac{\partial^2 \theta_F}{\partial r^2} - \frac{1}{r} \frac{\partial \theta_F}{\partial r} \right] + G_F = \rho_F C_F \frac{\partial \theta_F}{\partial t} \qquad (1)$$

The power density (G_F) is a function of position and time. The spacial variation of G_F is represented by the radial and axial power profiles of the core. Also a local peaking factor is included to account for power variations within a fuel channel. That is, different rods in a single fuel channel may have different power densities at the same axial location. The radial and axial profile and the local peaking factors are assumed to be independent of one another and independent of time. With these definitions and assumptions the power density (G_F) may be written:

$$G_F = G_{AVE} \left(P_R P_A P_L \right) \tag{2}$$

Where: P_R is the radial peaking factor for the fuel channel in which the power is being calculated. In other words, the radial peaking factor is uniform within any one channel. The local peaking factor (P_L) accounts for power variations within the channel. P_A, the axial peaking factor, is taken from the axial power profile of the core. Finally, G_{ave} is the core averaged power density and is a time variable. The time variation is represented by experimentally determined neutron and decay power curves as a function of time.

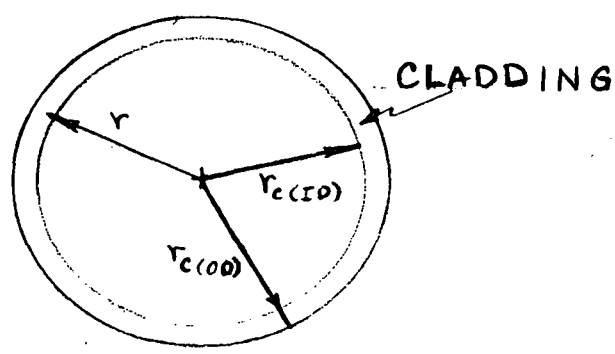
Substitute (2) into (1):

$$K_F \left[\frac{\partial^2 \theta_F}{\partial r^2} + \frac{1}{r} \frac{\partial \theta_F}{\partial r} \right] + G_{AVE} \left(P_R P_A P_L \right) = \rho_F C_F \frac{\partial \theta_F}{\partial t} \tag{3}$$

Equation (3) describes the temperature at any point in the fuel at any time anywhere in the core.

Consider now the fuel cladding, Figure 2.

Figure 2 - Fuel Cladding



An equation similar to equation (1) for the fuel may be written for the cladding.

$$K_c\left[\frac{\partial^2\theta_c}{\partial r^2} + \frac{1}{r}\frac{\partial\theta_c}{\partial r}\right] = \rho_c C_c \frac{\partial\theta_c}{\partial T} \qquad (4)$$

Note it is assumed there is no power production in the cladding. Equation (4) describes the temperature at any point in the cladding at any time anywhere in the core.

Consider now the boundary conditions between the fuel and the cladding. This interface is considered to present a thermal resistance (H_I). The boundary conditions at the inside surface of the cladding may then be written as:

$$\left[\theta_{F(OD)} - \theta_{c(ID)}\right] H_I = -K_c \frac{\partial\theta_c}{\partial r}\Big|_{r = r_{c(ID)}} \qquad (5)$$

Similarly, for the outside surface of the fuel we may write:

$$\left[\theta_{F(OD)} - \theta_{c(ID)}\right] = -K_F \frac{\partial\theta_F}{\partial r}\Big|_{r = r_{F(OD)}} \qquad (6)$$

The boundary conditions at the outside surface of the cladding are considerably more complicated. Here convective heat transfer, radiative heat transfer and possible chemical energy from a metal water reaction are taken into account. The condition at this boundary are given by:

$$-K_c \frac{\partial\theta_c}{\partial r}\Big|_{r = r_{c(OD)}} = h\left(\theta_{c(OD)} - \theta_G\right) + \sigma \sum_n F_{c-n}\left[\theta^4_{c(OD)} - \theta^4_n\right] - \left[\text{NET CHEM ENERGY RATE}\right] \qquad (7)$$

The convective heat transfer coefficient (h) is dependent upon the thermo-hydraulic process occurring in the channel. It is determined by independent calculation for any particular case under consideration. The temperature of the coolant occupying the channel θ_G, is taken as the saturation temperature of water at the pressure existing in the channel. This temperature, then, depends also upon the thermo-hydraulic process occurring in the channel and is determined by independent calculations. This temperature is assumed to be independent of location; varying only with time.

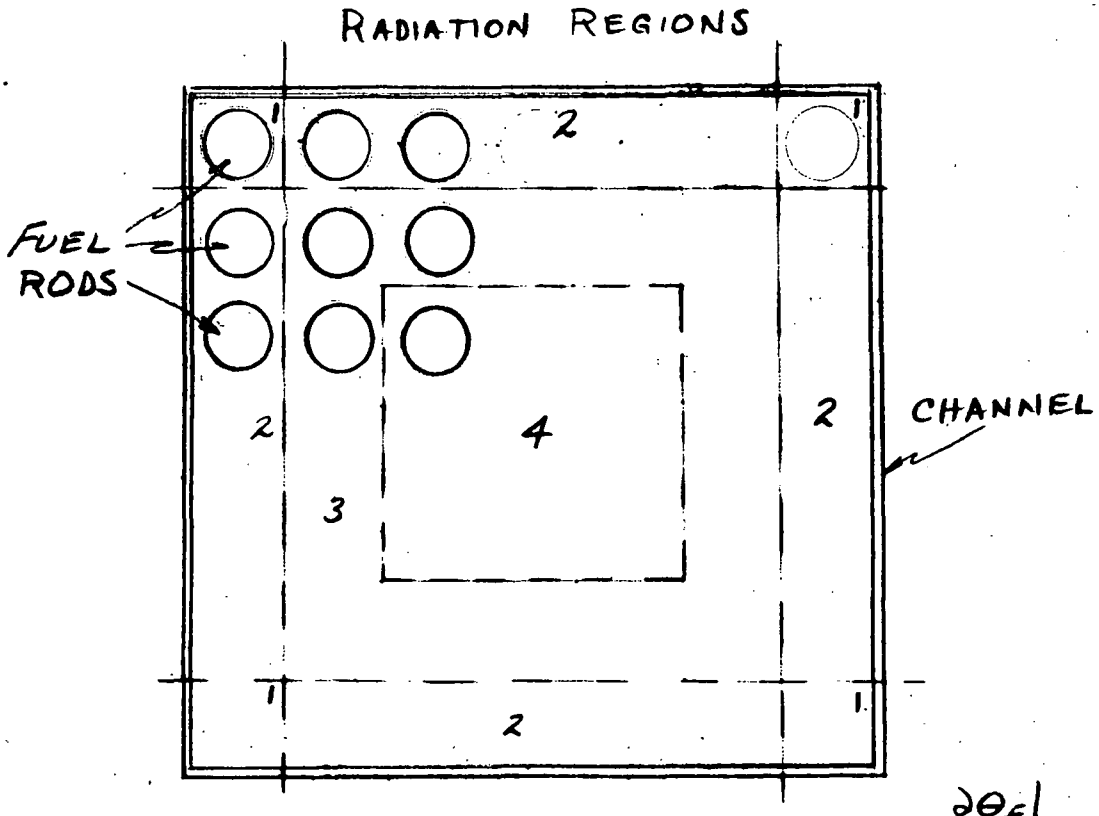
The grey body form factor (F_{c-n}) is determined from the following equation:

$$F_{c-n} = \frac{1}{\frac{1}{f_{c-n}} + \left(\frac{1}{\epsilon_c} - 1\right) + \frac{A_c}{A_N}\left(\frac{1}{\epsilon_n} - 1\right)} \qquad (8)$$

The emissivities of all bodies in the channel are taken as equal and constant with temperature and time. The black body from factor is difficult to determine and is estimated for each geometry under consideration. As a practical matter no fuel clad is considered to exchange radiation with more than two other bodies. For instance, consider Figure 3: The rods in the corner regions (1) exchange radiation with the channel, the rods in the peripheral regions (2) exchange radiation with the channel and the rods in region 3. Region 3 rods exchange radiation with rods in regions 2 and 4. For this case other radiation exchanges are neglected as relatively small.

The chemical energy term will be discussed subsequently.

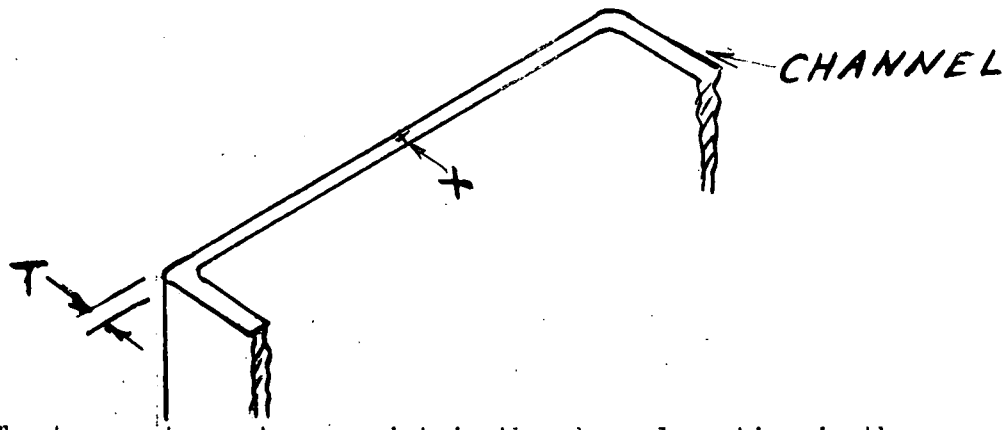
Figure 3



One additional boundary condition is written for the fuel that is $\left.\frac{\partial \theta_f}{\partial r}\right|_{r=0} = 0$ (9)
In other words, the temperature gradient at the center of the fuel is zero.

The entire channel surrounding the fuel rods is considered to be at one temperature at any axial position.

Figure 4



The temperature at any point in the channel section is then:

$$K_{CH} \frac{\partial^2 \theta_{CH}}{\partial x^2} = \rho_{CH} \, C_{CH} \frac{\partial \theta_{CH}}{\partial T} \tag{10}$$

The boundary conditions on the channel are:

At the inside surface:

$$-K_{CH} \frac{\partial \theta_{CH}}{\partial x}\bigg|_{x=0} = h\left[\theta_{CH(x=0)} - \theta_g\right] + \sigma \sum F_{CH-N}\left[\theta^4_{CH(x=0)} - \theta_n^4\right] - \text{[NET CHEM. ENERGY RATE]*} \tag{11}$$

and at the outside surface:

$$\frac{\partial \theta_{CH}}{\partial x}\bigg|_{x=T} = 0 \tag{12}$$

Equations (3), (4), and (10) described the temperature distributions in the fuel, cladding and channel respectively. Equations (5), (6), (7), (9), (11), and (12) give boundary conditions sufficient for the solution of equations (3), (4) and (10).

Still another condition must be considered. The fuel cladding generally will melt at a significantly lower temperature than the fuel and it is desirable to produce

*Chemical reaction is based on reaction taking place on both sides of channel.

calculations which extend to fuel melt temperatures. Before this happens, much of the cladding will have melted. In melting, the metal will absorb its latent heat of fusion. This fact is accommodated by setting the temperature of the metal at melt temperature until it has absorbed the latent heat of fusion and then letting the temperature rise again.

Finally, with the governing equations, the temperature distribution in the core is determined by integration of these equations. Finite difference methods are employed in a digital computer as a practical means of performing the integration.

2. Chemical Energy Determination

We will now consider the chemical energy terms included in equation (7) and (11). In general, metals at high temperatures are oxidized by water. This oxidation reaction produces free hydrogen gas and exothermal energy. For instance, consider the zirconium reaction with water:

$$Zr + 2H_2O \longrightarrow ZrO_2 + 2H_2 + \text{Energy} \tag{13}$$

An exposed core can present conditions under which this reaction can take place, namely, metal, high temperature and water. If it is assumed that sufficient water is available, the rate of reaction of zirconium, specifically, can be calculated from the following equation:

$$\frac{d\ th_{ox}}{dt} = k_1 \frac{e^{-\frac{k_2}{\theta_s}}}{th_{ox}} \tag{14}$$

The form of this equation was established by experiments and reported in Reference 1.

The local hydrogen production rate may be written:

$$(\text{Local Hydrogen Production Rate}) = k_3 \frac{d\ th_{ox}}{dt} \tag{15}$$

and the gross energy production rate may be written:

$$(\text{Gross Chemical Energy Rate}) = k_4 \frac{d\ th_{ox}}{dt} \tag{16}$$

Equation (16) does not yield the net chemical energy rate directly. Exothermal energies for equation (13) are quoted under the assumption that the reagents and products are at the same temperature. It is not reasonable to assume that the water entering into the reaction is available at the elevated temperature of the metal.

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(1) Louis Baker, Jr. and Louis C. Just, "Studies of Metal-Water Reactions at High Temperature III. Experimental and Theoretical Studies of Zirconium - Water Reactions", ANL6548 Argonne National Laboratory, 1962.

4. Nomenclature

r	Radius	In
ρ	Density	#m/In^3
G	Power Density	Btu/sec # m
d	Differential Operator	———
C	Heat Capacity	Btu/# M ^oF
θ	Temperature	^oF
t	Time	Sec
K	Thermal Conductivity	Btu/Sec In. ^oF
P	Peaking Factor	———
H	Thermal Resistance	Btu/Sec In^2 ^oF
h	Convective Heat Transfer Coefficient	Btu/Sec In^2 ^oF
σ	Stefan-Boltzmann Constant	Btu/Sec^2 (^oF)^4
F	Grey Body Form Factor	———
f	Black Body Form Factor	———
ϵ	Emissivity	———
A	Area	In^2
T	Channel Section Thickness	In
k_1	Metal Water Reaction Constant	in^2/sec
th	Thickness	In
k_2	Metal Water Reaction Constant	^oK
k_3	Proportionately constant between oxide production and hydrogen production	$\frac{\#m}{In}$
k_4	Proportionately constant between oxide production and chemical energy production	Btu/In

5. Subscripts

F	Refers to the fuel
OD	Refers to the outside dimension
Ave.	Core Averaged
R	Radial
A	Axial
L	Local
C	Refers to the fuel cladding
ID	Refers to inside dimensions
I	Refers to the fuel-cladding interface
n	Refers to any body "viewed" by the cladding
G	Refers to coolant occupying the channel
CH	Refers to the channel
S	Refers to a metal surface
OX	Refers to a metal oxide

III HIGH PRESSURE COOLANT INJECTION SYSTEM

III. 1. Design Basis

The following Design Basis has been adopted for the High Pressure Coolant Injection System and has, therefore, served as the basis for evaluating the adequacy of the provided system.

1. The High Pressure Coolant Injection System is provided to insure adequate coolant inventory in the reactor vessel for a spectrum of coolant loss conditions. Adequate coolant inventory is defined as that amount of coolant necessary to maintain uninterrupted complete core cooling. The spectrum of coolant loss conditions for which this injection system can provide adequate inventory control up to and including the lower range of breaks which are adequately protected by the core spray system.

2. The High Pressure Coolant Injection System shall meet the above design basis requirements without reliance on external power supplies to the injection system or the reactor system. Thus condenser how well inventory is considered unavailable.

3. The High Pressure Coolant Injection System shall be designed so that each component of the system can be tested on a periodic basis.

III.2 SYSTEM DESIGN

III.2.1 Description

The High Pressure Coolant Injection System is designed to pump water into the reactor vessel under loss of coolant conditions associated with reactor high pressure. The loss of coolant might be due to loss of AC power and therefore, loss of reactor feedwater, or due to small line breaks which do not cause immediate depressurization of the reactor vessel.

The high pressure coolant injection system consists of a steam turbine driven makeup pump, valves, high pressure piping, water sources and instrumentation. The high pressure coolant injection piping and instrumentation is shown in Figure III-2-1. The turbine drive pump units are located in the reactor building as shown in Figure II-2-5.

The turbine is driven with extraction steam off the main steam lines. Two sources of cooling water are available for the High Pressure Coolant Injection System. Cooling water will be initially supplied from a condensate storage tank. When the condensate storage tank has fallen below a pre-determined level or the suppression pool has raised above a predetermined level the pump suction supply will be automatically transferred to the suppression pool. This transfer may also be made by the operator. Approximately 90,000 gallons of water are held in reserve in a condensate storage tank for supply to the HPCI System. Water from either source will be pumped into the reactor vessel through the feedwater line at a rate of approximately 5600 gpm. Flow will be distributed within the reactor vessel through the feedwater sparger to obtain mixing with the hot water in the reactor pressure vessel. Water leaving the vessel through a line break drains by gravity back to the suppression pool. The containment cooling

system is required for cooling of the suppression pool after several hours of operation of the HPCI system.

Primary system relief valves, rated at 40% of throttle flow, are used in conjunction with the HPCI to relieve pressure in the reactor pressure vessel. Under conditions of isolation with no line breaks, these relief valves will automatically open periodically to control reactor pressure.

With small line breaks, operator action to control the reset point of these valves provides the back-up for depressurization of the reactor pressure vessel to a pressure level at which core spray is effective. Discharge from these valves is piped to the suppression pool for condensation.

The high pressure coolant injection system is designed to pump 5600 gpm into the reactor pressure vessel within a reactor pressure range of about 1100 psig to 150 psig. As the pressure decreases, the turbine throttle valves open up to pass the required steam flow to match the pump power which is proportional to pressure. The size of the system is selected on the basis of providing sufficient core cooling to prevent fuel melting. The system is designed to depressurize the reactor pressure vessel to the point at which core spray is effective.

Operation of the system is from reactor water level. Low water level starts the system and high level will stop it. The steam supply to the unit will include a moisture separator ahead of the turbine to permit rapid start of the unit without warmup. Turbine speed is controlled by the turbine governor system.

Exhaust steam from the unit is discharged to a drain tank where the moisture is collected and drained to the suppression pool through a trap. Steam is discharged to the suppression pool from this tank through a pressure

regulating valve that holds the turbine exhaust pressure to approximately 10 psi above suppression chamber pressure.

The turbine gland seals are vented to the gland seal condenser. Water from the pump is routed through the condenser for cooling purposes. Non-condensible gases from the gland seal condenser are ducted to the reactor building exhaust system. Under conditions of high radioactivity in the reactor building, this exhaust system is isolated and vented through the standby gas treatment system.

Operation of the HPCI system is completely independent of AC power and required only DC power from the station battery to operate.

The piping of the system is designed to ASA B 31.1 and the pumps to Section VIII of the ASME Code. Arrangement of the piping includes considerations for potential damage. Open runs will be protected by structural steel. Testing and inspection of this piping will be equal to that required for the primary containment system.

The isolation condenser will perform the function of core cooling under conditions of reactor isolation with no loss of water from the vessel other than by relief valve action. This system is actuated by high reactor pressure and continues to operate to remove decay heat and eventually depressurize the reactor. Back-up to the isolation condenser is obtained by intermittent blowdown through the relief valves with water level maintained by the High Pressure Coolant Injection System.

III.2.2 System Control

The circuit is arranged to provide independent and separately isolated control and power circuits for operation of the two independent systems, i.e., the high pressure injection as one system and the isolation condenser as another independent system. The P & ID is shown in Figure III-2-1.

The Block Diagram is shown in Figures III-2-2 and III-2-3.

a. High Pressure Injection System Automatic Initiation

1. Initiation by reactor vessel low level, detected by four independent low level sensing switches A, B, C, D, connected in one out of two twice array.

2. The following valves open upon system initiation:

   Valve MO. 19 - Steam supply to turbine pump - 20 sec. maximum (Normally open)

   Valve MO. 12 - Pump discharge startup valve to reactor vessel (Concurrent)

3. The following valves receive the initiation signal to open even though they are maintained open during normal process:

   Valve MO. 30 - Maintenance valve upstream of pump discharge valve

   Valve MO. 20 - Isolation valve in steam supply to turbine pump

   Valve MO. 2 - Pump suction from condensate storage tank (Concurrent).

4. The minimum flow by-pass valve 32 opens when pump suction valve MO.2 opens.

b. High Pressure Injection System Automatic Shutdown

When reactor level rises to the normal operating level, the low level sensing switches will initiate closure of the valves MO. 19, MO. 12, to shut down the system. The valves MO. 20, MO. 30 and MO. 2 remain open.

c. Standby Water Supply From Suppression

1. Chamber Pool

   In the event of low water level in the condensate storage tank or high water level in the suppression chamber, level switches 34 and 36 initiate the closure of valve MO. 2 and the opening valves MO. 4 and MO. 6 to provide pump suction from the suppression pool.

The Block Diagram is shown in Figures III-2-2 and III-2-3.

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1. Initiation by reactor vessel low level, detected by four independent low level sensing switches A, B, C, D, connected in one out of two twice array.

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   In the event of low water level in the condensate storage tank or high water level in the suppression chamber, level switches 34 and 36 initiate the closure of valve MO. 2 and the opening valves MO. 4 and MO. 6 to provide pump suction from the suppression pool.

2. Concurrent with the closure of MO.2, the minimum flow by-pass valve 32 closes.
3. Concurrent with opening valves MO. 4 and MO. 6, the minimum flow bypass valve 33 to the suppression pool opens.

d. System Testing

The pump may be tested at full flow at any time except when reactor water level is low, the condensate storage tank water level is low, or the pressure suppression suction valves MO. 4 and MO. 6 are not closed, as follows:

1. Valve MO. 2 and MO. 32 are open to provide pump suction from the condensate storage tank.
2. Open steam supply valve MO. 19 by remote manual switch to start pump and establish minimum flow.
3. Open test by-pass valve MO. 31 to establish full rated flow from pump through by-pass line to condensate storage.

The valves may be tested as follows:

4. With pump off and valve MO. 30 closed, valve MO. 12 may be stroken open and closed by remote manual switch.
5. The testable check valve MO. 14 may be stroked open and closed at any time except when valve upstream pressure is lower than reactor pressure.

Reactor Low Water Level Simultaneous With Test

1. In the event reactor low water level occurs while system is being tested, the system is automatically restored to the automatic start-up status.

Isolation Condenser

1. The steam valve MO. 25 and MO. 26 and the condensate return valve MO. 28 are maintained open during normal operation.

2. In event of high reactor pressure, four independent pressure switches connected in one out of two twice array initiate opening of valve MO. 27 to permit steam flow to the isolation condenser.
3. Shutdown of the system after initiation and when reactor pressure is normal may be done manually by remote manual switch.
4. Automatic shutdown of the system is initiated only upon evidence of line break as detected by excess flow detection switches DPIS A,B,C,D. Excess flow indicated by any one switch will initiate closure of the isolation valves MO. 25 and MO. 28 and the operational valves MO. 27 and MO. 26.
5. System may be tested by manual opening and closing the system valves by their independent remote manual switches.

III.2.3 Design Provisions to Test Performance of Components

Provisions have been designed into the core spray system to test the performance of its various components, described as follows:

1. Instrumentation

Plant pre-operational test of entire system.

Periodic system tests using test lines.

2. Valves

Plant pre-operational test of entire system.

Periodic system tests using test lines.

Leak-off lines between isolation valves.

Drainline on pump side of outboard isolation valves.

Safety valves can be removed and tested for set point.

Motor-valves can be exercised independently.

3. Pumps

   Plant pre-operational test of entire system.

   Periodic system test lines.

   Pump seal leakage is monitored.

4. Spray Sparger

   Plant pre-operational test of entire system.

5. Spray Nozzles

   Plant pre-operational test of entire system.

6. Relief Valve

   Can be removed and tested for set point.

7. Screens

   Plant per-operational test of entire system.

   Periodic system test using test lines.

   Pressure indicator or pump suction during above tests.

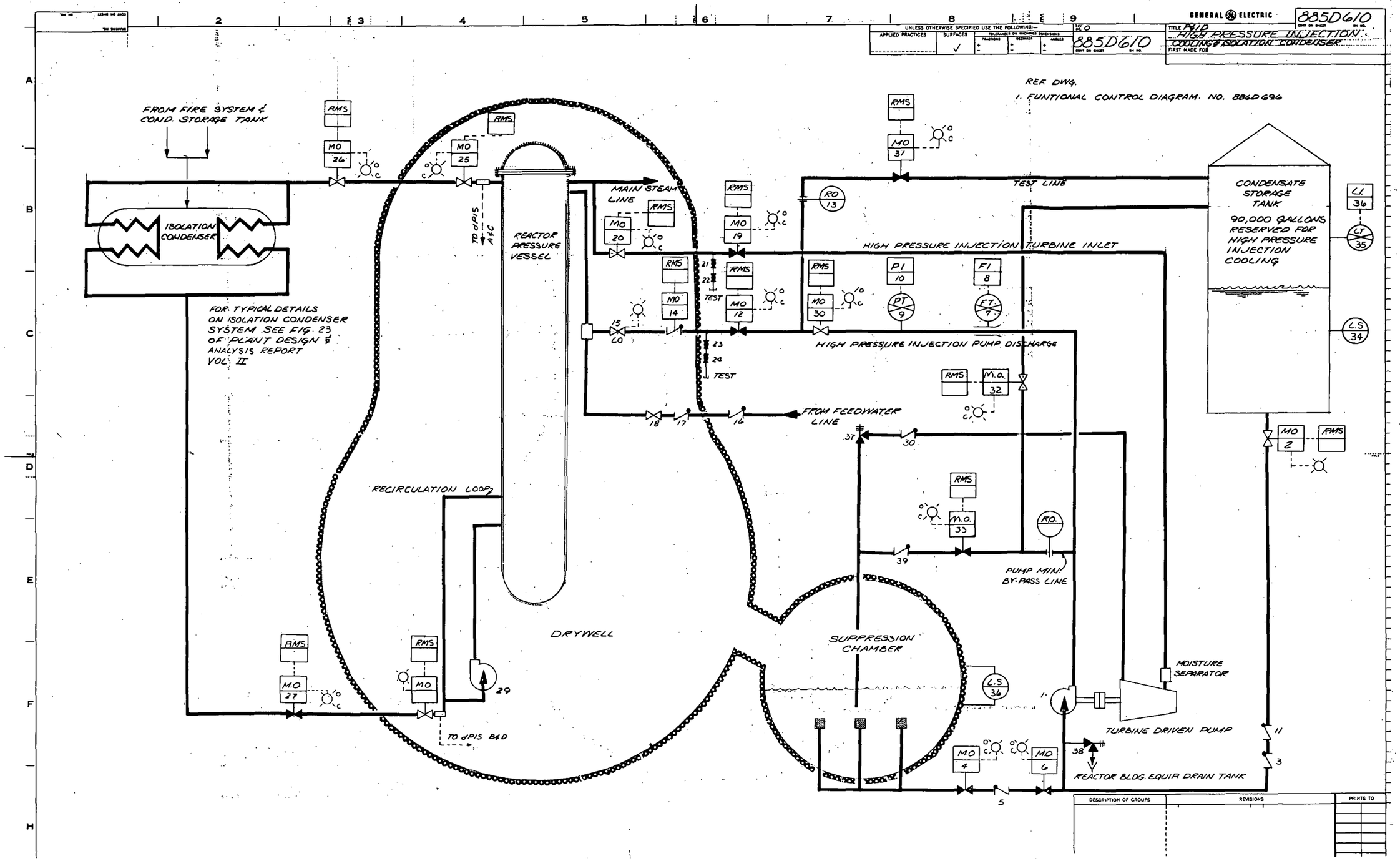


FIGURE III. 2-1

GENERAL ELECTRIC

886D696 CONT ON SHEET 2 SH NO. 1

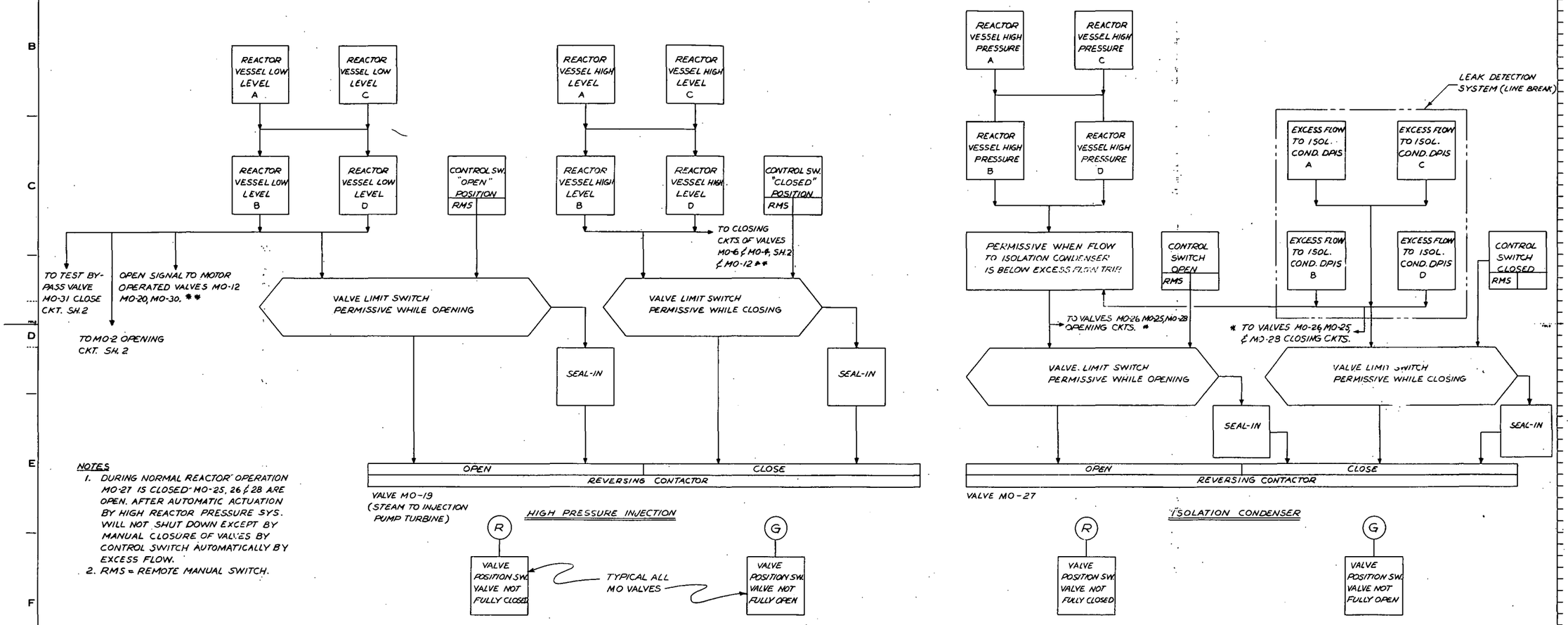
UNLESS OTHERWISE SPECIFIED USE THE FOLLOWING:— APPLIED PRACTICES | SURFACES ✓ | TOLERANCES ON MACHINED DIMENSIONS: FRACTIONS ± | DECIMALS ± | ANGLES ±

REV 0 886D696 CONT ON SHEET 2 SH NO. 1

TITLE FUNCTIONAL CONTROL DIAG. HIGH PRESSURE INJECTION & ISOLATION CONDENSER

FIRST MADE FOR ISOL. CONDENSER SYS. [redacted]

REF. DWG
1. 885D610 P&ID HIGH PRESS. INJ. & ISOL. COND.



NOTES
1. DURING NORMAL REACTOR OPERATION MO-27 IS CLOSED. MO-25, 26 & 28 ARE OPEN. AFTER AUTOMATIC ACTUATION BY HIGH REACTOR PRESSURE SYS. WILL NOT SHUT DOWN EXCEPT BY MANUAL CLOSURE OF VALVES BY CONTROL SWITCH AUTOMATICALLY BY EXCESS FLOW.
2. RMS = REMOTE MANUAL SWITCH.

** OPERATING CIRCUITS FOR MO-12, MO-30, MO-20, ARE SIMILAR TO MO-19

* MO25, MO26 & MO28 VALVE CKTS. ARE SIMILAR TO VALVE MO-26.

PRELIMINARY

DESCRIPTION OF GROUPS	REVISIONS	PRINTS TO

FIGURE III. 2-2

GENERAL ELECTRIC

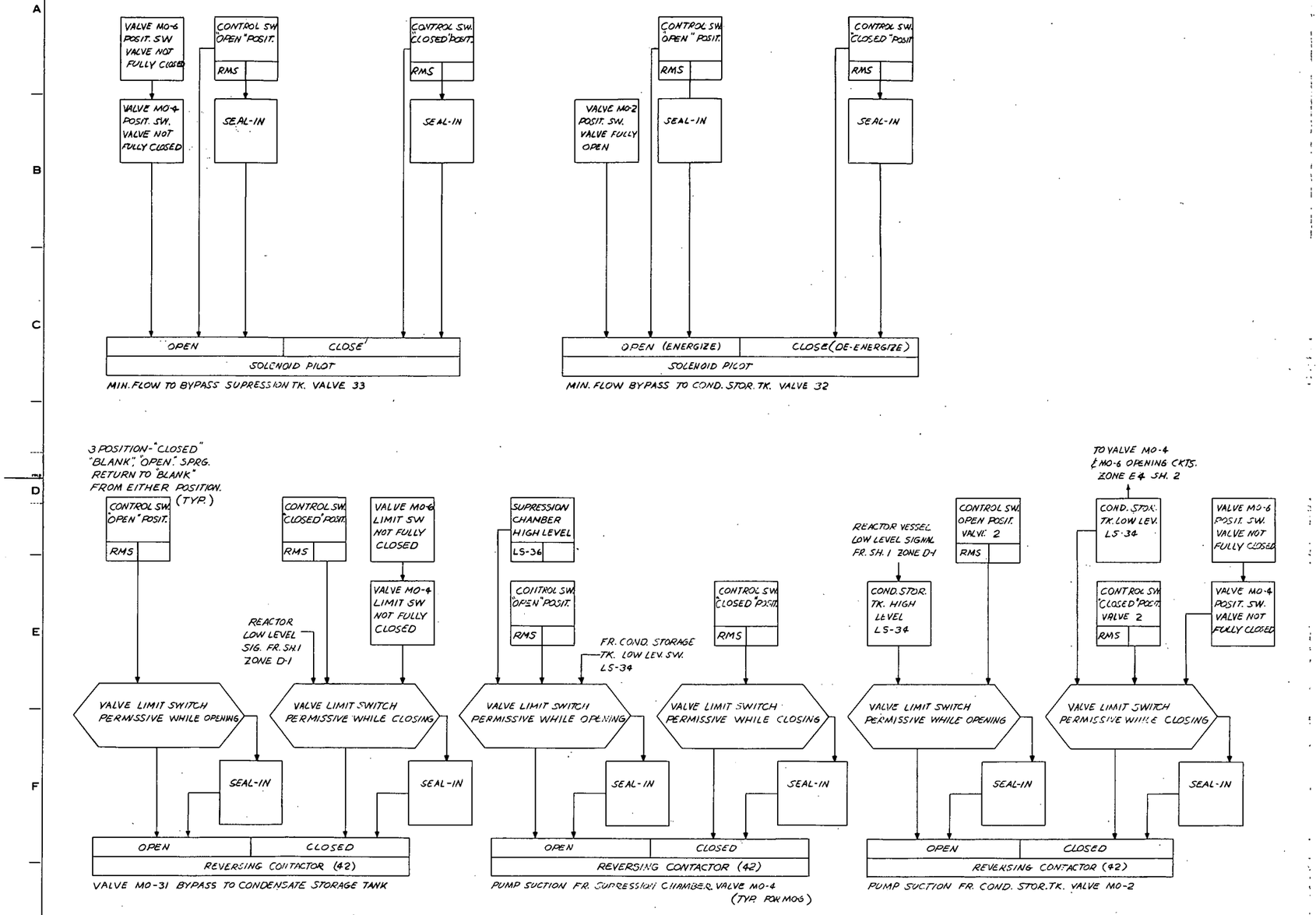
886D696 DGMT OR SHEET — SH. NO. 2

UNLESS OTHERWISE SPECIFIED USE THE FOLLOWING:— APPLIED PRACTICES | SURFACES ✓ | TOLERANCES ON MACHINED DIMENSIONS: FRACTIONS ± | DECIMALS ± | ANGLES ±

REV. NO. 0 — 886D696 DGMT OR SHEET — SH. NO. 2

TITLE FUNCTIONAL CONTROL DIAG. HIGH PRESSURE INJECTION ISOLATION CONDENSER

FIRST MADE FOR ISOL. CONDENSER SYS. [redacted]



PRELIMINARY

DESCRIPTION OF GROUPS	REVISIONS	PRINTS TO

FIGURE III. 2-3

III. 3. SYSTEM EVALUATION

III.3.1 Introduction

The High Pressure Coolant Injection System has been evaluated with respect to several major areas of concern. The first of these is the reliability of the system to be available when needed. Second, given system availability, the performance of the system is examined to assure that the design basis is met. This examination considers the structural integrity of the system to withstand the effects of an accident for which the system is required to be available. The suitability of valve, pump and turbine sequencing, speed of operation and capacity to accommodate the design basis is also considered.

III.3.2 High Pressure Injection System Availability

To inject water at high pressure, three major active components must operate. A motor-operated valve must open to admit steam to the turbine driving the pump, a motor-operated valve must open to admit the discharge flow from the pump into the reactor feedwater line, and the turbine driven pump itself must operate. When the supply of water in the condensate storage tank is exhausted, two more motor-operated valves must open and one close so that the pump draws water from the suppression chamber rather than the condensate storage tank.

A final detailed availability analysis of the high pressure injection system has not been carried out at this time in the design cycle. However, several observations can be made regarding the nature of the components and the over-all system objectives which will play an important role in the analysis when it is performed. First of all, the turbine driving the pump is designed especially for this type of service. It operates over a wide range of inlet and exhaust pressures, and the construction is such that it can start cold and come to full power operation almost instantaneously. There is always steam pressure available to drive this pump whenever high pressure injection is needed. The system can be tested frequently so that failures would be detected early.

The whole operation is automatic and requires no manual intervention. When the condensate storage tank is pumped down to a low level, the pump suction is automatically transferred to the torus.

The high pressure injection system and the core spray system compliment one another. The high pressure coolant injection system protects against the small breaks. The core spray system protects against large breaks and in addition automatically takes over from the high pressure injection system when the steam pressure falls below the minimum level required to operate the turbine.

There are many actions the operator can take to prevent core damage for moderate size breaks. If normal sources of power are available, he can continue to operate the regular feed-water pumps to provide make-up. He can transfer water from the condensate storage tank to the hotwell so that this type of cooling can be continued for a longer period of time. Finally, he can manually depressurize the vessel so that core spray will provide cooling.

For emphasis, it must be restated that operator action is not required except in the event of failure of the high pressure injection system following a break within a specific range of sizes.

III.3.3 Performance Evaluation

The auxiliary feedwater system performance evaluation is based in part upon an analytic prediction over the entire range of variables of importance. The analytic model used in making these predictions is described in Appendix III-A. Briefly, this model employs an energy and mass balance on the primary coolant to establish the state within the primary vessel at any time. Fixed volume and saturated conditions throughout are assumed. The several flows to and from the vessel, such as leak flow, feedwater, main turbine steam and auxiliary feedwater are included. The core decay heat and sensible heat of the core vessel and internal structure are included. In all, a detailed simulation is achieved.

System inventory is maintained despite losses through the leak. Leak integrity of the internal shroud is not required for inventory maintenance or core cooling.

While this model includes the feedwater flow, this flow is ignored after scram has occurred in the basic evaluation of the auxiliary feedwater system. In addition, it is assumed that a break in the primary system has occurred. The flow from this break causes the drywell to pressurize until a scram is initiated by high drywell pressure. For breaks of interest, the feedwater controller will be able to maintain normal reactor inventory. Thus, the model begins its prediction with the reactor at normal pressure, normal coolant inventory and normal power

with a scram and loss of feedwater flow having occurred at time zero. One of the results of this calculation of interest is level in the vessel. Level is defined as the fraction of the active core which would be below the liquid if all the liquid and vapor in the system were separated. Using this definition of level it has been shown experimentally that if half the active core is covered, the core can be adequately cooled. Therefore, one method of meeting the design basis is to not let level at any time be less than half of the active core. A second method of protecting the core is to cause the system to depressurize so that core spray can reach rated flow. This must be done without level going below half of the core long enough to cause core damage. The analytic model predicts the variables of interest to determine if the method is effective. The level variable shows when and if the core is not adequately cooled. The pressure variable shows when rated core spray is achieved and core cooling reestablished. The length of time which the core is uncooled must be less than the permissible uncooled times shown in Figure II.3.7-2.

Consider a sample evaluation. For this evaluation, the break size is taken as 0.8 ft^2 and the quality of the flow from this break is arbitrarily taken to be 0.5. Beginning at normal coolant inventory, and having just scrammed and lost feedwater flow, the model predicts the results shown in Figures III.3.3-1 through III.3.3-3.

Figure III.3.3-1 shows the system pressure as a function of time. As is to be expected, the insertion of cool water and loss of inventory rapidly depressurizes the system. When pressure reached 165 psia the injection system turbine stops due to low inlet pressure. However, this pressure is below the shutoff head of the core spray pumps. Therefore, when the injection turbine stops, the core spray will be at 70% of rated flow. This core spray flow rate will continue the depressurization until pressures which permit rated or greater core spray flow are reached.

Figure III.3.3-2 shows the masses of liquid and vapor in the system. Liquid mass decreases rapidly due to high leak flow. As system pressure falls the leak flow falls and finally the inventory begins to increase. In time liquid mass reaches a steady state at which the liquid is at normal water level. Steam mass initially increases due to an increased volume made available by loss of liquid. As pressure falls, the decrease in steam density outweights the effect of steam volume increase and steam mass decreases.

Finally, Figure III.3.3-3 shows the level in the system as a function of time. The initial level drop is due to the rapid liquid mass loss. In general, level closely follows liquid mass except for the specific volume change with time affects the liquid volume. It should be noted that at no time was core cooling inadequate even though the cooling function was transferred from the High Pressure Coolant Injection System to the Core Spray System.

This basic calculation is repeated over a range of break sizes and break flow qualities with the objective of determining the break areas and leak qualities which the coolant injection system can assure will not result in overheating the core. The results of these calculations are shown in Figure III.3.3-4.

This figure shows that the maximum break area which can be accommodated by the coolant injection system is 0.16 ft^2, assuming liquid only passes through the break. This result is to be expected since at any particular pressure the inventory loss rate from the system is greatest for liquid only flow. The leak size which can be accommodated increases rapidly with leak quality.

The region in the lefthand region of this Figure shows the condition under which system pressure would attempt to exceed normal operating pressure. At these high pressures, either the isolation condenser or the relief valve will be used to maintain permissible system pressure. This region in the plot extends to zero leak areas over the entire quality range. If there is no leak, the High Pressure Coolant Injection System operates as a make-up if relief valves are used. In this model of operation the injection system and relief valves act as an energy removal system for the primary system.

III.3.4 Summary

The high pressure injection system will maintain water inventory sufficient to assure core cooling for small breaks. For larger breaks it will cause depressurization as well as helping to maintain liquid inventory. This depressurization will enable the core spray to function before core damage can occur.

III.3.5 Feedwater Sparger Integrity

The general philosophy and criteria used in the mechanical design of the feedwater sparger is discussed in Section II.3.2 which included a discussion of the core spray integrity. The following paragraphs cover some of the features unique to the feedwater sparger.

Four feedwater spargers are utilized in a reactor. Each sparger is approximately 70° in arc length and mounted to the inside reactor vessel surface. The sleeve is attached to sparger midpoint. The spargers are mounted to the vessel at one elevation such that they distribute the feedwater in a symmetric pattern about the vessel axis. As pointed out previously the thermal sleeve is not welded to the vessel nozzle. Because of this feature, the feedwater sparger is removable. Vibration consideration for feedwater sparger is the same as that discussed on page II.3.3-10.
Each sparger is supported by the thermal sleeve and a bracket mounted to each end of the sparger. Provision is made for the differential expansion between the stainless steel sparger and carbon steel vessel. Radial differential expansion is taken up by the slip fit of the thermal sleeve into the vessel nozzle. Tangential differential expansion is taken up by tangential slots cut in the bracket mounted to each end of the feedwater sparger bracket. The sparger is conservatively analyzed assuming the thermal sleeve is welded into the nozzle. Additionally, pressure differentials, jet reactions, and earthquake loadings are all added such that the stresses within the sparger are all within ASME Code Section III for Class A Vessels.

The resultant bracket loads mounted on the vessel are given to the vessel vendor in the format issued drawing such that the vessel brackets can be sized so that they too meet the Section III criteria. Vendor analysis on the brackets are checked and must be approved by General Electric before design is accepted.

III.3.6 Timing Sequence

The system must (1) initiate operation automatically when required and (2) be in operation within the required time.

The reactor level sensor devices are the same type as used for the core spray and reactor protection systems.

The sequence of automatic operator and the time delays involved are described in Section III.2 and Figure III.3.6-1. Immediately on receipt of signal the valves are opened and steam admitted to the turbine.

Continuous Operation

Operation of the HPCI turbine will continue as long as reactor pressure is above 150 psig. Components can be set up to maintain constant speed as the pressure is reduced. This will be determined from more detailed evaluation of the turbine system requirements.

The system will automatically maintain water level between low level and normal level if the break size is within the capacity of the pump and the reactor is not depressurized below 150 psig.

Termination

When the pressure has fallen below 150 psig, the speed of the turbine-pump unit will begin to decrease and will gradually be slowed to a stop by friction and windage losses. Core cooling at this time will be accomplished by the core spray system, or, for a small break, maintained by the control and rod drive supply pumps if AC power is available.

III.3.6 QUALITY ASSURANCE

Materials

The materials specified for the HPIC system, from the reactor up to and including the second motor operated valve, are stainless steel in accordance with an appropriate ASME material specification as follows:

Pipe - ASTM A312, A 376, or A358.

Fittings, Flanges and Valves ASTM A183, A336, A351, or A403.

The materials specified beyond the second motor operated valve are carbon steel, in accordance with an appropriate ASTM material specification as follows:

Pipe - ASTM A106 Gr. B or A155 Gr KC70 Class I, Firebox quality.

Fittings, Flanges and Valves: ASTM A105, Gr II, A216 Gr WCB or A234 Gr WPB

Fabrication and testing of the piping and valves up to and including the second isolation valve will be in accordance with ASME Pressure Vessel Code Section I; the remaining piping and valve in accordance with ASA B31.1. Additional Requirements specified for the weld joint design and welding procedure for piping 2 1/2" and larger in size are as follows: (1) Welds are required to be full penetration groove welds. Double welded joints are required to be inspected for soundness prior to welding the back side.

Single-welded joints are required to be made by using (a) U-groove and consumable insert ring with Gas Tungsten Arc Weld process for root and second layer; (b) for carbon steel base metal, U or VEE groove with Gas Metal Arc Welding process and short circuiting transfer (short arc) for root and second layer; or (c) U, VEE, or bevel groove with metal backing which is subsequently removed and the weld inspected for soundness on the back side.

Single welded joints, in stainless steel portion of the system, made by the Gas Tungsten Arc Weld process, without metal backing are required to have a protective gas back purge for the root and second layer.

Inspection and Tests

Additional General Electric inspection and tests specified beyond the code requirements are as follows:

Castings:	(1)	Radiograph and either liquid penetrant or magnetic particle examination.
Forgings:	(2)	All forgings - liquid penetrant or magnetic particle examination. Forgings 4 inch thickness-ultrasonic examination.
Welds		All welds - Either liquid penetrant or magnetic particle examination.
	(2)	Pressure containing groove welds - Radiographic examination.
Valve Stems		Ultrasonic and liquid penetrant examination.
Bolting		Pressure containing - Either liquid penetrant or magnetic particle examination.

The system is hydrostatically tested at 1 1/2 times the design pressure except those portions forming an integral part of the containment. These portions will be tested in accordance with the ASME Boiler and Pressure Vessel Code, Section III.

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(1) Beyond the 2nd isolation valve. Only valves four inches and larger are required to be radiographed. Liquid penetrant or magnetic particle are not required.

(2) Applicable to components 2 1/2" and larger size.

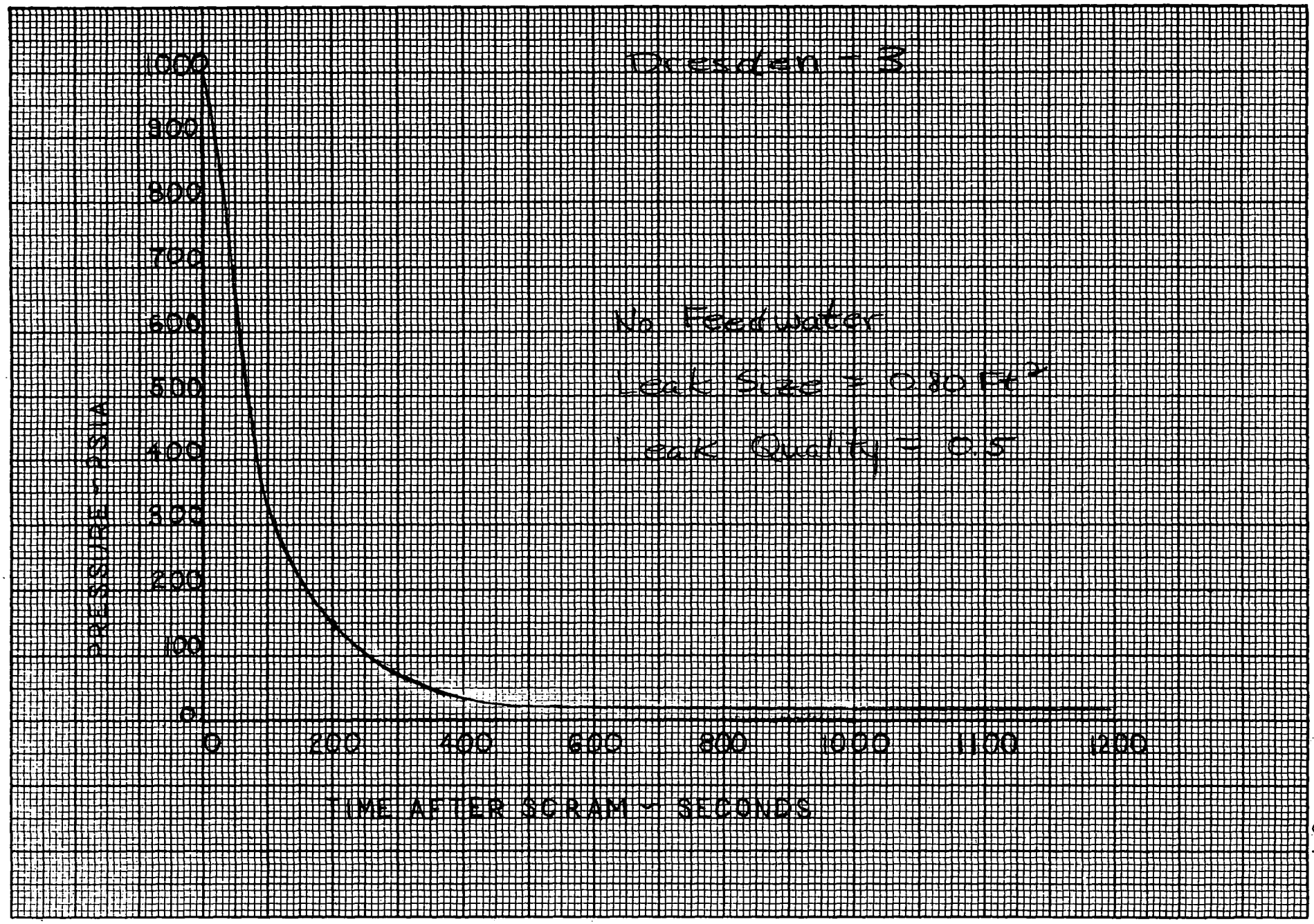


Figure III.3.3-1

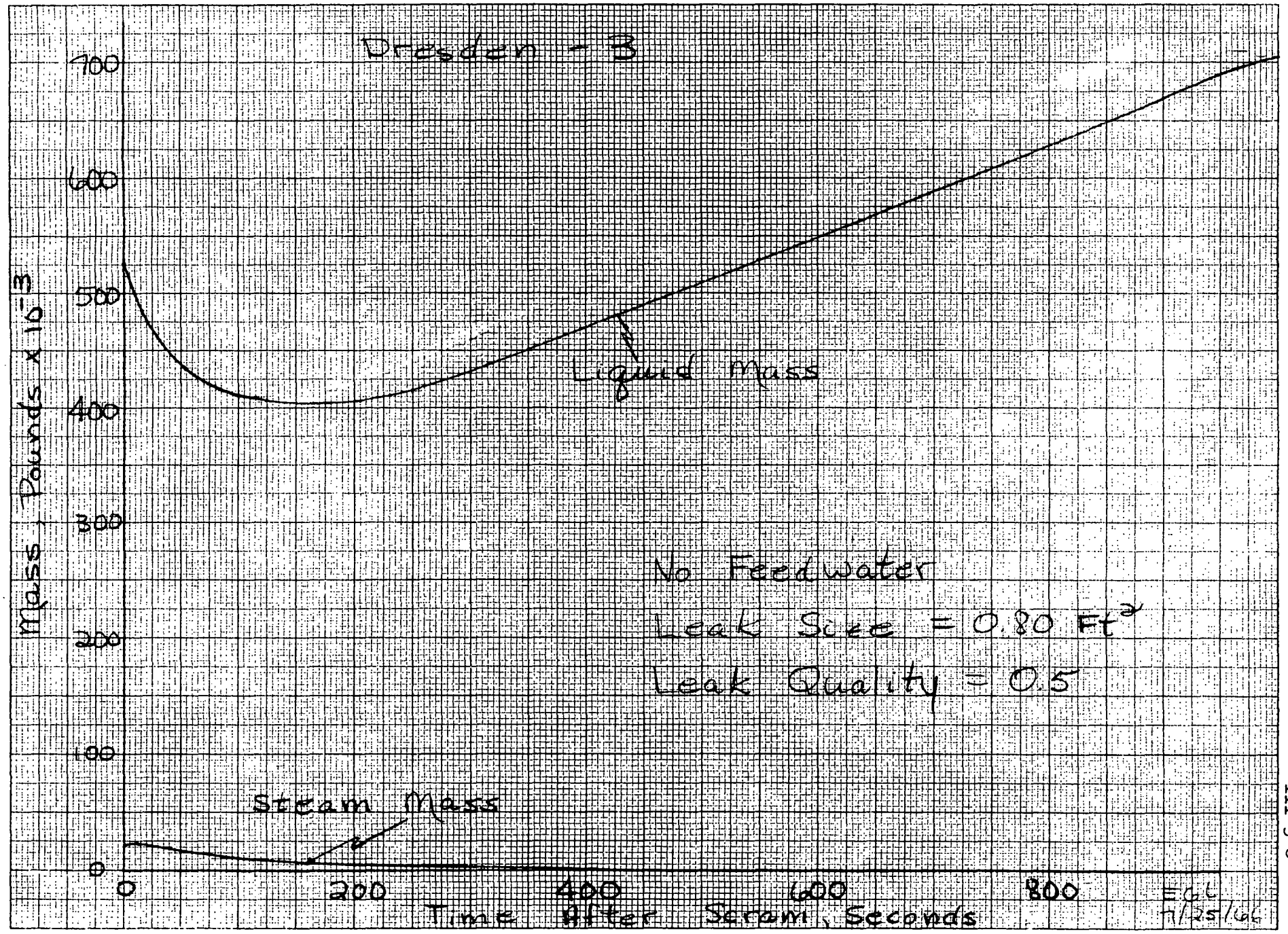


Figure III.3.3-2

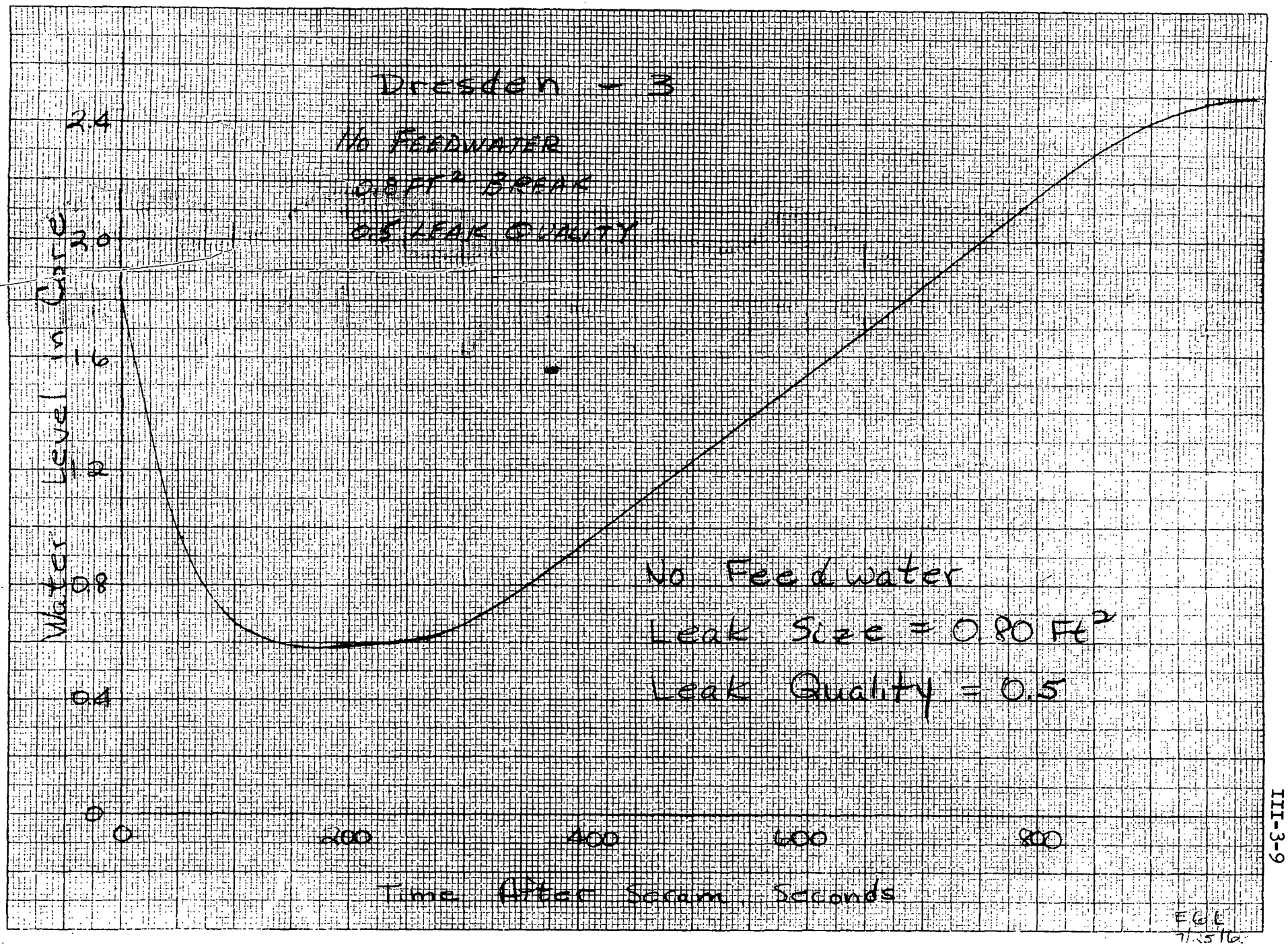


Figure III.3.3-3

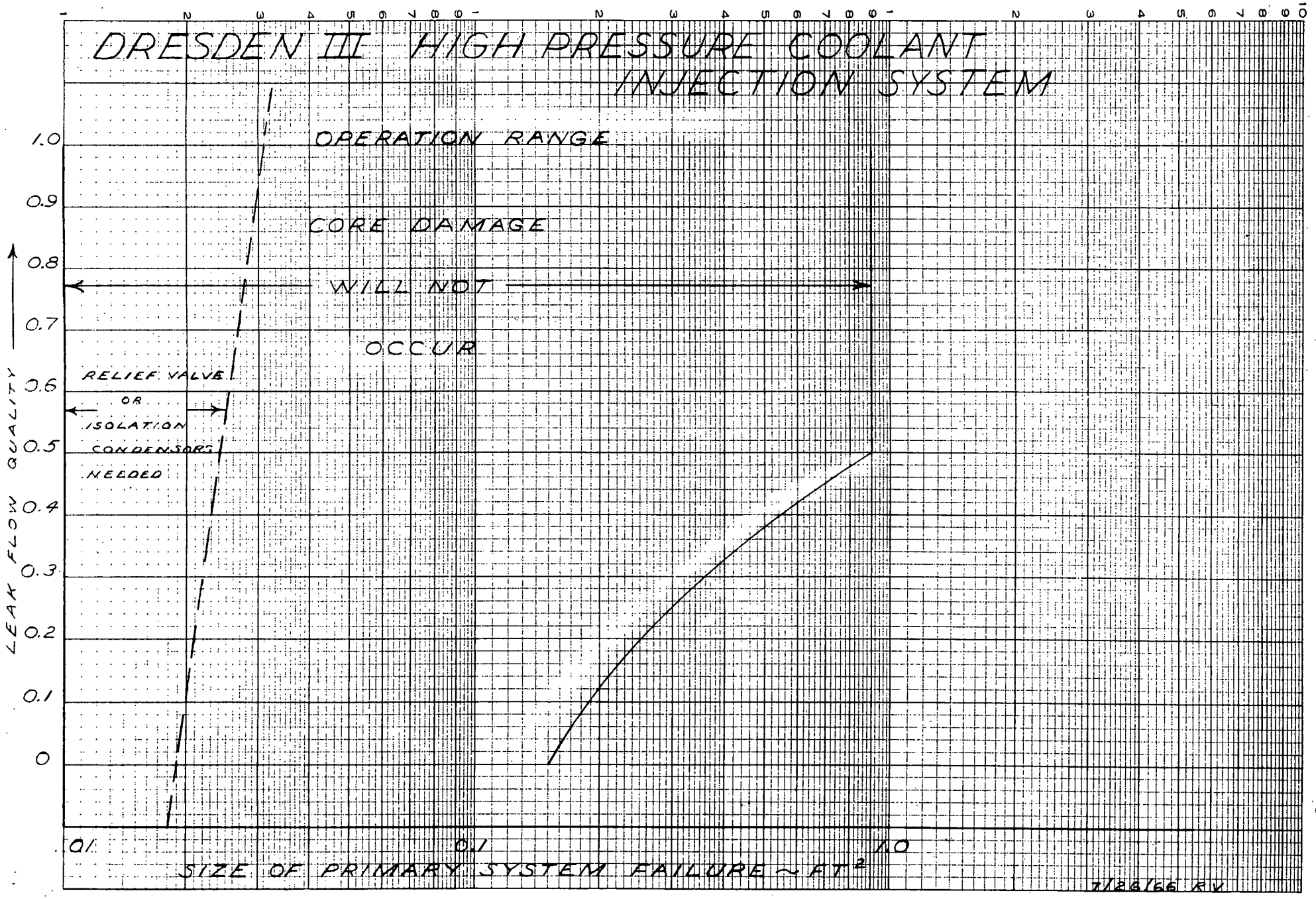
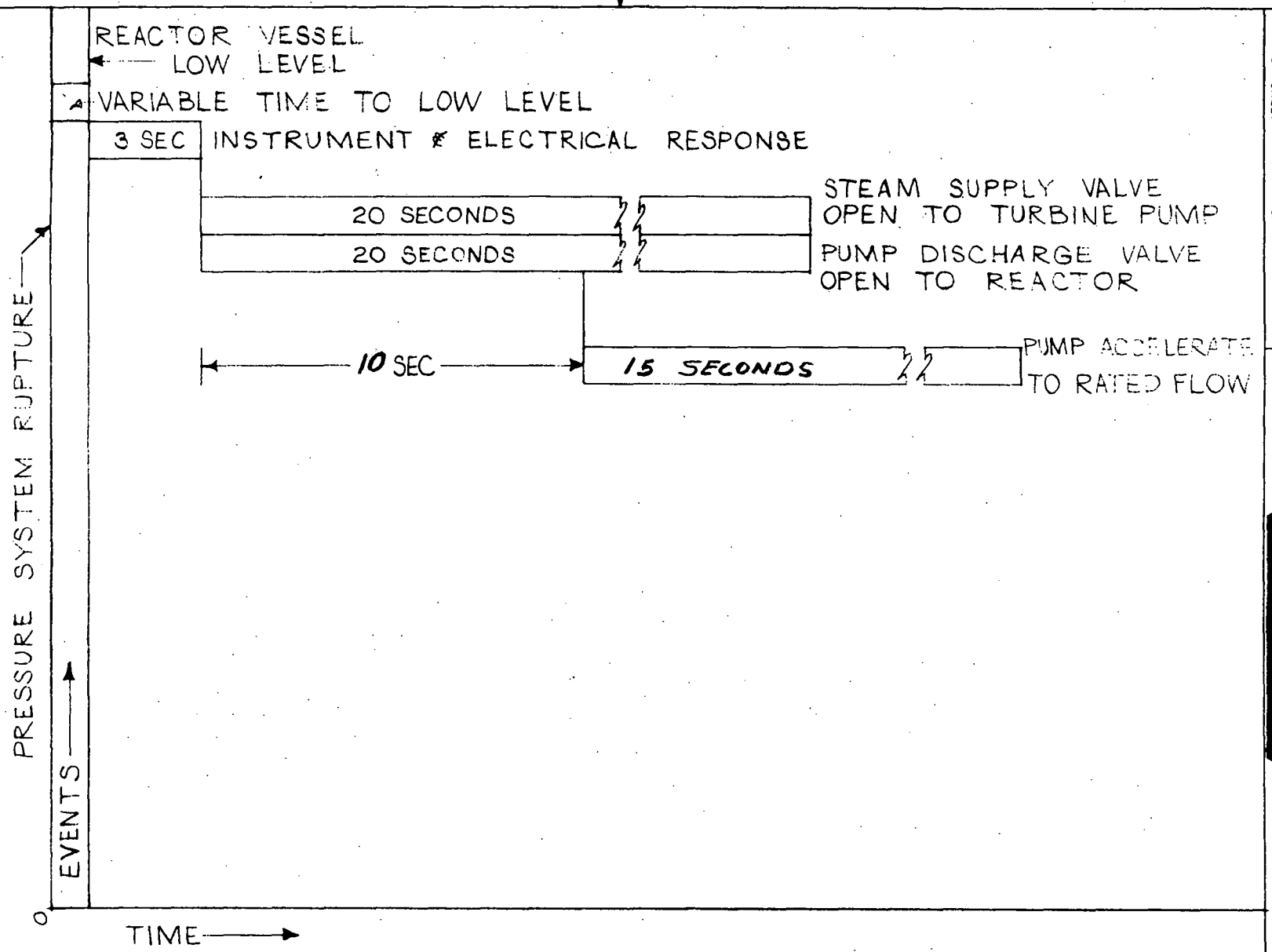


Figure III.3.3-4



HIGH PRESSURE INJECTION RESPONSE TIME WITH LOSS OF COOLANT

FIGURE III.3.6-1

III-3-11

GENERAL ELECTRIC

TITLE HIGH PRESSURE INJECTION SYSTEM (RESPONSE TIME)

FIRST MADE FOR [redacted]

REV NO. 0

CONT ON SHEET SK5612-929 SH NO.

MADE BY Tomm JULY 26 1966

ISSUED

APPROVALS

DIV OR DEPT. APED

LOCATION SAN JOSE

CONT ON SHEET SK5612-929 SH NO.

CODE IDENT NO.

PRINTS TO

REVISIONS 0

FF-803-WA (9-64) PRINTED IN U.S.A.

III.4 Surveillance and Testing

To assure that the high pressure injection cooling system will function properly if it is needed, specific provisions are made for testing the operability and performance of the various parts of the system. This testing will be close at a frequency that will assure availability of the system. In addition, surveillance features will provide continuous monitoring of vital portions of the system.

The following paragraphs detail the testing and surveillance that can be accomplished during the different modes of operation of the plant.

1. Prior to Plant Start-up

   a. Prior to plant start-up, a pre-operational test of the complete high pressure injection cooling system will be conducted. This test will utilize steam to drive the turbine from an external source. All components of the system will be checked during this test including the turbine, pump, valves and instrumentation.

   b. Suction will be taken alternately from the condensate storage tank and from the torus to assure the proper operation of both sources.

   c. This pre-operational test will verify that the system meets its design objectives and will also furnish reference characteristics such as pressure differentials and flows that can be used as base points for check measurements in subsequent testing of this system.

2. Unit Operating or at "Hot Standby"

   a. A system test up to the isolation valve can be conducted. The steam admission valve is opened, driving the turbine-pump unit at its rated output. The valves from the torus and to the feedwater line remains closed and water is pumped from the condensate storage tank, through the system, and returned to the condensate tank by way of the test line.

   b. To assure proper operation of the valves and strainers when pumping from the torus, the turbine-pump unit will be run at a reduced rate by throttling at the turbine and pumping from the torus and returning the flow back to the torus by way of the minimum by-pass line. Electrical interlocks will be provided to prevent the pumping of torus water into the condensate storage tank.

Appendix III-A

Reactor Coolant Inventory Model

1.) Introduction

This appendix describes the analytic model used to predict the performance of the different systems used to maintain reactor coolant inventory or core cooling. In simplest form, the model determines the water mass in the system and the energy in the system at any time. With these facts and assuming the system water is at saturation throughout and of fixed volume, the pressure and steam quality are implicitly known. Several auxiliary formulations are required to predict the several mass flows to and from the system, such as feedwater flow and flow from leaks. Similarly, several energy flows must be predicted, such as core decay power and enthalpy fluxes associated with mass flows.

Finally since core cooling can depend upon the level within the vessel, level is predicted knowing the mass and pressure in the system along with the physical dimensions of the system.

2.) Basic Equations

The system is defined in this model as the liquid and vapor water in the reactor vessel, recirculation lines and steam lines from the vessel to the isolation valves. Expressly, not included within the system are the vessel, core materials and internal structures.

1. For this system, a conservation of mass equation yields:

$$M = M_0 + \int_0^T \left(\sum_m w_j\right) dt$$

2. A conservation of energy equation yields:

$$E = E_0 + \int_0^T \sum_m (hw)_j \, dt + \int_0^T \sum_m Q_k \, dt$$

3. The average internal energy of the system is:

$$\bar{e} = \frac{E}{M}$$

4. The average specific volume of the system is:

$$\bar{v} = \frac{V}{M}$$

The system is assumed to be at saturation throughout; therefore, equations 3 and 4 yield sufficient state functions to determine all other state functions in the system. For instance, consider the following:

5. $e = e_f + e_{fg} X$

6. $v = v_f + v_{fg} X$

Eliminate quality between 5 and 6.

7. $$\frac{e - e_f}{e_{fg}} = \frac{v - v_f}{v_{fg}}$$

Since saturation is assumed, e_f, e_{fg}, v_f and v_{fg} are functions of pressure on temperature only. Equation 7 implies a system pressure or temperature ~~or~~ OR any other state function.

Thus, if equations 1 and 2 are evaluated, the state with the system is known. The evaluation of these equations requires several auxiliary equations and relations.

3.) <u>Auxiliary Equations</u>

Consider first the several mass flow rates to the system:

<u>Leak Flow (W_L)</u>

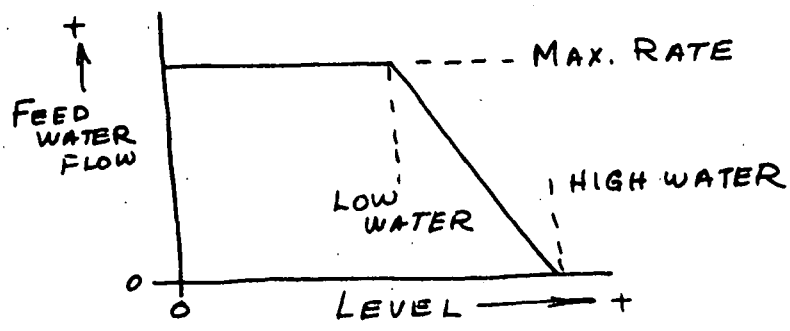
The leak flow rate is predicted by methods presented in reference 1. Briefly, these are:

8. $W_L = -GA_L$

Where G is a function of system pressure and stagnation enthalpy as predicted in Ref. 1. In any particular calculation, the stagnation quality hence the stagnation enthalpy can be stipulated arbitrarily or system averaged values used.

<u>Feedwater Flow</u> (W_a)

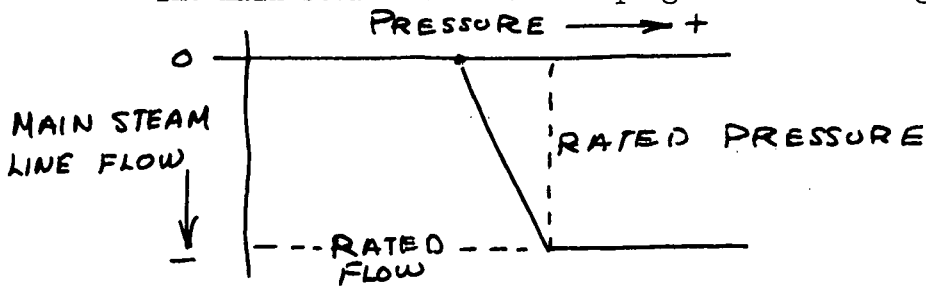
Feedwater flow is programmed according to the following figure:



This program simulates the level controler's control over feedwater flow. This flow is limited in duration by the condenser hotwell inventory.

Main Steam Time Flow (W_s)

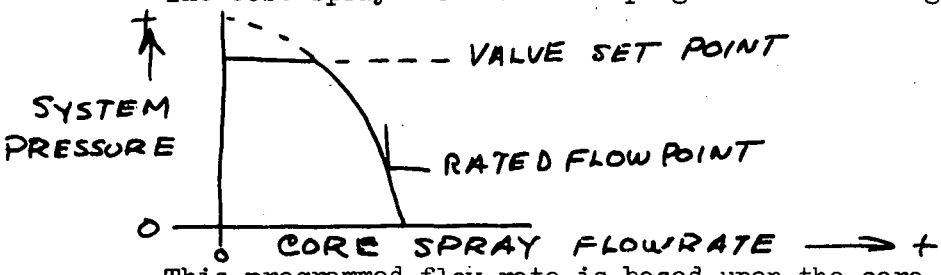
The main steam line flow was programmed according to the following figure.



This program simulates the initial pressure regulator's control over steam flow.

Core Spray Flow Rate (W_c)

The core spray flow rate was programmed according to the following figure.

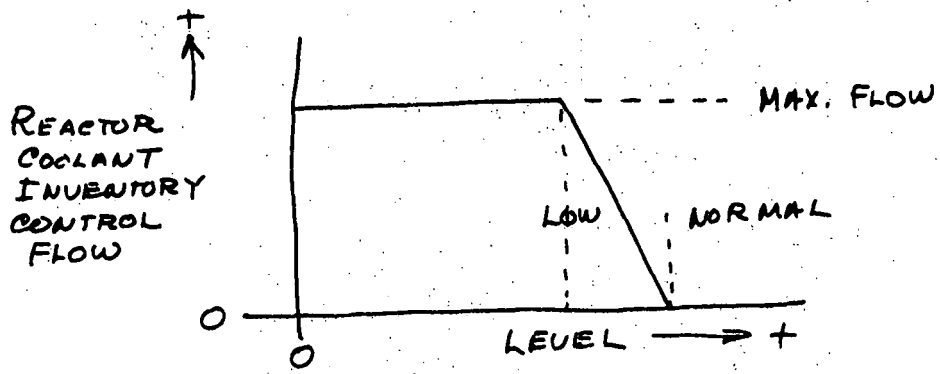


This programmed flow rate is based upon the core spray pump head flow characteristic, the elevation head changes and the irreversible head losses in the core spray system. As is to be expected as system pressure falls, core spray flow increases. A valve on the core spray line truncates this characteristic before shut of back pressure is reached. All core spray is contingent upon level being below low low water level.

4.) Reactor Coolant Inventory Control

Liquid Flow (W_R)

The turbine pump and control combination of this system have been matched so that the maximum flow is independent of system pressure until pressure reaches some low valve below which the system stops pumping. Therefore, liquid flow follows the following program.



This program simulates the level controlers demand to the RCIC system. Of course, this demand is honored only if other signals are permissive.

Steam Flow (W_b)

Steam flow to the RCIC turbine is reasonably approximated as proportional to the liquid flow from RCIC.

9. $\therefore \quad W_b = NW_R$

Substitute these flows in equation 1.

10. $M = Mo + \int_0^T (W_L + W_a + W_s + W_c + W_R + W_D)\,dt$

It is this equation which is integrated.

Consider now the enthalpy of the several flows:

Leak enthalpy (h_L)

The leak enthalpy is predicted as follows:

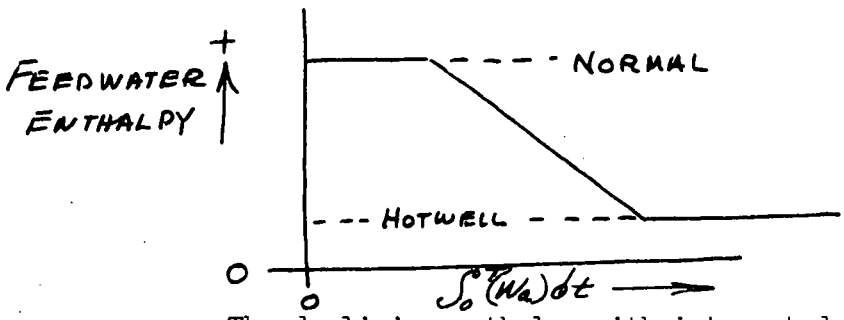
11. $h_L = h_f + h_{fg} X$

Where: X may be arbitrarily assigned or system averaged.

Feedwater enthalpy (h_a)

Feedwater enthalpy depends upon condenser hotwell temperature and the amount of heating accomplished by the feedwater heaters. Under normal conditions the feedwater enthalpy is constant but after steam flow to the turbine stops feedwater heating of necessity deminishes.

Enthalpy after steam flow stops is programmed according to the following figure.



The declining enthalpy with integrated flow simulates the effect of cool down of the feedwater heaters.

Steam Line Flow Enthalpy (h_s)

The enthalpy of the steam line flow is simply the enthalpy of steam in the system.

12. $\therefore h_s = h_g$

Core Spray Enthalpy (h_e)

Core spray enthalpy is taken to BE the enthalpy of the suppression pool which is assumed to be constant over the period of time of interest.

RCIC Liquid Flow Enthalpy (h_R)

The RCIC liquid enthalpy flow is taken to be the enthalpy of the suppression pool which is assumed to be constant over the period of time of interest.

RCIC Steam Flow Enthalpy (h_b)

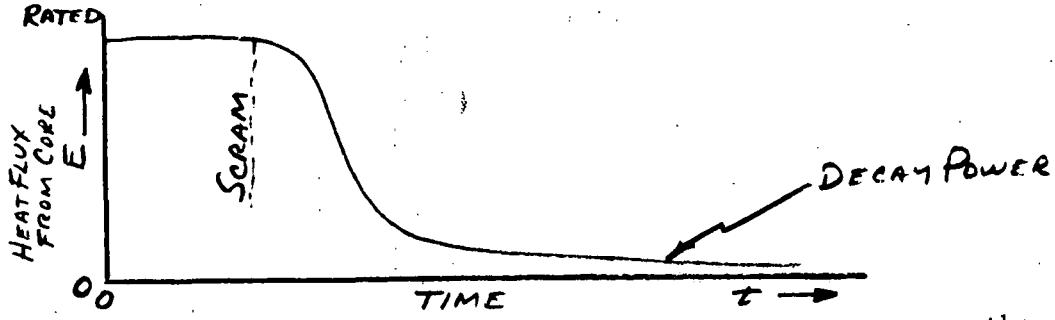
The RCIC steam flow enthalpy is simply the enthalpy of the steam in the system.

13. $\therefore h_b = h_g$

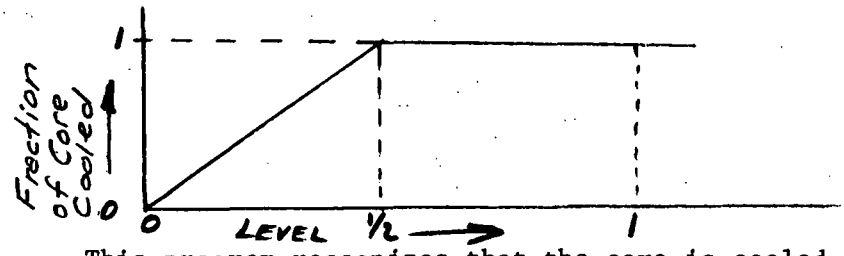
Consider finally, the heat fluxes into the system. These heat fluxes are divided into three terms. First, there is neutron and decay power. Second, upon scram or loss of neutron moderation the temperature gradient in the core cannot be supported and the core cools to system temperature. Thus, cooling releases sensible energy ~~to~~ FROM the core. The third term is the energy sensible energy released by the core, vessel internal and vessel when the system temperature falls.

Heat Flux from Core (Q_c)

The first two terms have been combined in a signal heat flux. This flux is programmed according to the following figure.



It is apparent that if coolant is not present in the core, the core heat flux must be zero. It is shown in Appendix 2-C that if the heat is above the first half of the core, the core will be cooled. Hence, the core cooling is programmed according to the following figure.



This program recognizes that the core is cooled if level is half way up the core and uncooled if level is below core. The intermediate ramp is assembled as reasonable.

The heat flux from the core then becomes:

14. $Q_d = JF$

<u>Sensible Heat Flux</u> Q_h

As the system cools, the vessel, core and internal structures will be cooled by the system. The resulting heat flux (Q_h) is approximated by the following equation:

15. $Q_h = -(\sum mc) \frac{d\theta}{dt}$

Where (mc) is the total heat capacity of the masses which are cooled by the system. This formulation is at best an approximation but can be conservative since it can be made to predict more heat transfer than is possible for any particular accident. Note heat transfer coefficient and area are ignored. Overestimating this heat flux has the effect of increasing system pressure which makes effective functioning the core spray and RCIC more difficult.

These terms are now substituted into 2.

16. $$E = E_0 + \int_0^T (w_L h_L + w_a h_a + w_s h_s + w_c h_c + w_R h_R + w_D h_D)\,dt + \int_0^T (Q_d + Q_h)\,dt$$

Several of the above equations and programs depend upon level. Level is defined as the fraction of the active core which would be covered if the liquid and vapor where separated. The equation for level is:

17. $$L = \frac{M_{TC} - V_{TAC}}{V_{TAC} - V_{BAC}}$$

where: V_{TAC} is the system volume below the top of the active core.

V_{BAC} is the system volume below the bottom of the active core.

5.) <u>Summary</u>

The preceeding set of equation and programs are sufficient predict inventory and state of the inventory in the system. Actual solutions are accomplished by digital computers.

A. Assumptions

1. System volume is constant.

2. System is at saturation throughout its volume.

B. Nomenclature

M	System Mass	#m
T	Current valve of time	sec
W	Mass flow	#m/SEC
t	time	sec
E	System Energy	BTU
h	Enthalpy of mass flow	BTU/#m
$\bar{e}$	Average internal energy	BTU/#m
V	System volume	FT^3
x	Steam Quality	-
A	Area	FT^2
G	Critical Mass Flow Rate	#m/FT^2 sec.
N	Ratio of steam to liquid flow for RCIC system	
Q	Heat Flux	BTU/SEC
F	Core Heat Flux with Cooling	BTU/SEC
J	Fraction effective core cooling	-
θ	System temperature	°F
m	Mass	#m
c	Heat capacity	BTU/ (# °F)
L	Level	(fraction of active core) / length
V	Volume	FT^3

C. Subscripts

o Denotes initial valve (t = o)

j Denotes one of several flows to the system

C. (Cont'd)

f	Denotes a liquid water property
g	Denotes a steam property
fg	Denotes change in property upon phase change
L	Denotes system leak
a	Denotes feedwater
S	Denotes main steam line
c	Denotes core spray
R	Denotes RCIC liquid flow
b	Denotes RCIC steam flow
k	Denotes one of several heat fluxes
d	Denotes core
h	Denotes sensible heat flux

TAC Top of Active Core

BAC Bottom of Active Core

D. Superscripts

$\bar{}$ Averaged valve

E. References

F. J. Moody, "Maximum Flow Rate of a Single Component, Two-Phase Mixture," Journal of Heat Transfer, Trans. ASME, Series C, Vol. 87, P. 134.

IV. ACCEPTABILITY OF INTEGRATED PERFORMANCE OF EMERGENCY CORE COOLING SYSTEMS

Introduction

With respect to engineered safeguards for the postulated accidental loss of coolant conditions, the Dresden III nuclear power plant has been provided with a sufficient number of systems so that in the event of any of the postulated accidents adequate core cooling would be provided.

The postulated loss of coolant accidents for which adequate core cooling has been provided encompass the entire spectrum from the smallest detectable leaks of either liquid or steam up to and including a major primary system rupture equal in size to that caused by a double ended recirculation line break.

Adequate core cooling has been defined in this design basis as a continuity of cooling system after the accident so that the fuel in the reactor core never exceeds its melting temperature.

The continuous cooling to the reactor core is provided by at least two different systems or techniques even under accidental loss of coolant conditions for which the normal AC power has been interrupted and the emergency core cooling must be provided by the emergency power supply - the diesel. This design basis of providing at least two different provisions for core cooling has been satisfied for the entire range of break sizes.

Objective

It is the purpose of this section of the report to demonstrate that for the entire spectrum of primary system breaks by which the loss of coolants have been postulated adequate core cooling has been provided by at least two different methods even under conditions of no normal AC power supply. Each of the core cooling systems available under these accident conditions has been evaluated on an individual basis in order to determine the range of break sizes over which adequate core cooling has been provided. This information is now compiled in order to evaluate the overall protection.

Emergency Core Cooling Systems

The following emergency core cooling systems or techniques were evaluated in the overall investigation of adequate core protection under loss of coolant conditions:

1. Core Spray System:

   As fully described in Section II of this report, two full capacity core spray systems with redundant moving parts have been provided as an adequate core cooling system with a complete backup.

   This system operates at reduced reactor pressure level having been provided with low pressure pumps. On large primary system ruptures, the system rapidly depressurizes so either of the core sprays is capable of providing complete core protection.

For the system breaks on the smaller end of the spectrum of core spray system capability there are two considerations which influence the adequacy of core spray system cooling. These considerations are time for vessel depressurization and equilibrium pressure level of the vessel for emission of system decay heat energy. As fully described in Section II of this report, all breaks larger than 0.15 ft^2 for liquid blowdown and 0.32 ft^2 for steam blowdown cause a vessel depressurization within sufficient time to allow core spray to provide adequate protection without any core overheating. As further described in Section II the decay heat of the core must leave the vessel as either liquid or steam through the break by means of a pressure differential between vessel and containment. If the break is so small that the necessary pressure differential prevents adequate core spray flow due to the nature of the pump characteristic, protection would not be considered adequate. This range could be extended further in the small sizes by operator action to hold pressure down however.

The complete range of core spray capability is shown on Figure IV-1 for both liquid and steam flow loss of coolant conditions. Notice that for a liquid flow loss of coolant, either of the two systems with just one of its pumps operating can provide adequate core cooling protection from the largest break down to a break of only 0.15 ft^2 without any operator action and without the need for AC power.

Figure IV-1 also demonstrates the range of small breaks for which core spray offers adequate core cooling for various levels of decay heat as discussed above. Notice that if core spray was being relied on for core cooling even eight hours after full power operation, breaks as small as 0.1 ft^2 for steam flow and 0.037 ft^2 for liquid flow could be adequately handled by the either core spray system without any AC or operator assistance.

2. Core Flooding Phenomena:

Since the core spray systems bring inventory into the vessel after the assumed primary system break, a consideration of the capacity of the core spray systems has revealed that a considerable spectrum of break sizes are within the core cooling capability of the core spray systems from strictly a vessel flooding consideration. No consideration need be given to reactor internals in this case since the entire vessel is flooded. If the break is a liquid flow break, the two core spray systems could maintain adequate core cooling by flooding for breaks as large as 0.22 ft. If the break is a steam flow and therefore above the normal core level the flooding capability covers the largest breaks considered. On the small end of the spectrum the flooding capability of the core spray systems is set by the level of decay heat and the required vessel pressure level to pass this energy as liquid or steam out the break. Figure IV-1 indicates the

expected ranges for both liquid and steam flow. Operator action or high pressure coolant injection action to hold pressure down would extend the range to even smaller breaks than those indicated. If AC power were available even the shutdown cooling system could extend this range.

3. Feedwater Availability:

Although the design objective of providing at least two techniques of adequate emergency core cooling for any break size considered even without AC and thus no feedwater availability, it is of interest to consider the additional protection offered by the feedwater system. For a liquid flow loss of coolant below the water level the feedwater system could easily maintain adequate core cooling for breaks as large as one square foot. This ability to make up inventory would last for a sufficient time to bring the system pressure to atmospheric and thus allow core spray systems to operate. No dependency on this water supply was made in complying with the design basis of two systems or techniques for providing core cooling for all break sizes considered.

4. High Pressure Coolant Injection System:

The High Pressure Coolant Injection System provides another protection in the form of adequate core cooling for the critical range of very small primary system breaks. This system which has been fully described in Section III of this report is primarily an inventory control system, but also by the fact that it introduces cold water into the vessel to

maintain inventory, it is a depressurization system as well. The High Pressure Coolant Injection System has been sized to provide adequate core cooling for the smallest break up to and in excess of breaks covered by the core spray system. For a liquid flow break condition the High Pressure Coolant Injection System can provide adequate cooling for all breaks up to and including 0.16 ft^2, for steam flow breaks, 0.90 ft^2. The HPCI is capable of injecting coolant into the vessel at rated reactor pressure level and is powered by the vessel supplied steam turbine as described in Section III. Therefore, when the vessel is finally depressurized by HPCI action the HPCI system itself no longer operates. Adequate core cooling can then be provided by the low pressure systems. Figure IV-1 demonstrates the range of break sizes for which the HPCI provides adequate core cooling by inventory control. In all cases the system pressure finally would drop to a sufficiently low pressure to allow core spray operation. The duration of time from HPCI activation until sufficient system depressurization is a function of the break size. For the largest size break, almost 0.16 ft^2, which the HPCI system can handle by inventory control the system could possibly depressurize in as little as 400 seconds, thus turning core cooling duty over to either core spray. If it takes longer to depressurize the HPCI system would continue providing adequate core cooling by inventory control. For a break of about .05 ft^2 the depressurization would require in excess of an hour. During that time the HPCI system would be providing adequate core cooling by inventory control.

5. Operator Activated Depressurization

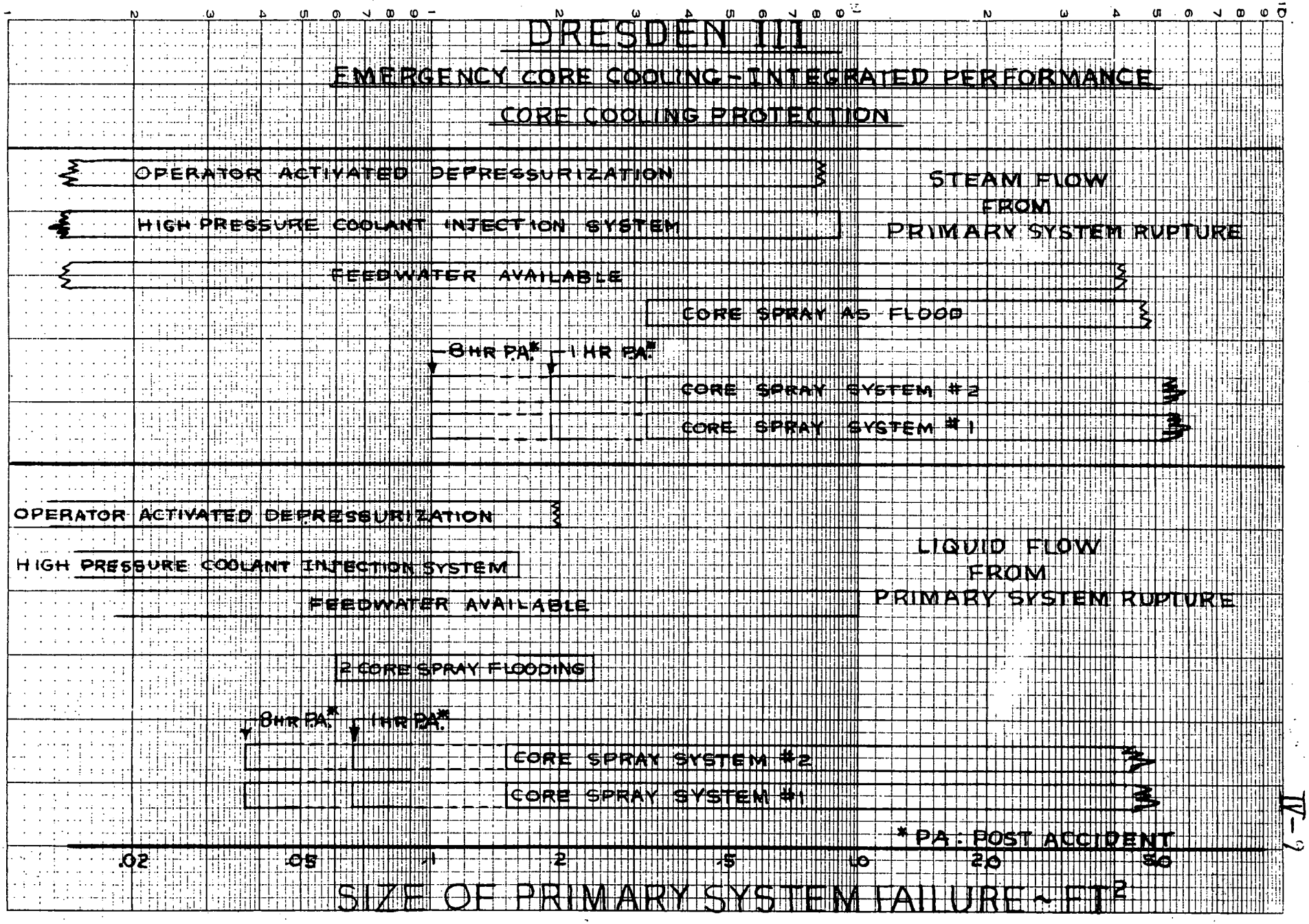
An additional procedure for providing adequate core cooling for various small line breaks is operator activated depressurization. There is sufficient relief capacity to drop the system pressure to a level at which core spray can provide adequate core cooling well within the allowable amount of time to prevent any core overheating. Even for a single phase steam blowdown through the relief valve and not accounting for any two phase blowdown the system can be depressurized within ten minutes thus allowing core spray to be activated and provide adequate core cooling. During depressurization core cooling would be provided, but even if it weren't, core overheating would not have occurred.

Evaluation of Integrated Behavior

The integrated performance of all of the provided core cooling systems is presented in Figure IV-1. The design basis required that for any size break under conditions of AC power being interrupted at least two independent core cooling systems could provide adequate core cooling. It should be pointed out that Figure IV-1 is presented on a log scale thus over emphasizing the very small break sizes. Notice that either core spray system can provide adequate core cooling from a size corresponding to a double ended recirculation line break about 5.5 ft^2 down to a break of only 0.15 ft^2. The additional systems are provided to offer protection for the line breaks which are even smaller than that range.

Notice that for all postulated line break sizes from the insignificant up to the design basis break of 5.5 ft^2 at least two systems or techniques were always available for providing adequate core cooling even if no AC power and thus feedwater supply were available. Thus the design basis for the emergency core cooling provisions has been satisfied.

Figure IV-1


DRESDEN III
EMERGENCY CORE COOLING - INTEGRATED PERFORMANCE
CORE COOLING PROTECTION
OPERATOR ACTIVATED DEPRESSURIZATION
HIGH PRESSURE COOLANT INJECTION SYSTEM
FEEDWATER AVAILABLE
CORE SPRAY AS FLOOD
8HR P.A.*
1 HR P.A.*
CORE SPRAY SYSTEM #2
CORE SPRAY SYSTEM #1
STEAM FLOW FROM PRIMARY SYSTEM RUPTURE
OPERATOR ACTIVATED DEPRESSURIZATION
HIGH PRESSURE COOLANT INJECTION SYSTEM
FEEDWATER AVAILABLE
2 CORE SPRAY FLOODING
8HR P.A.*
1HR P.A.*
CORE SPRAY SYSTEM #2
CORE SPRAY SYSTEM #1
LIQUID FLOW FROM PRIMARY SYSTEM RUPTURE
* PA: POST ACCIDENT
.02
.05
.1
.2
.5
1.0
2.0
5.0
SIZE OF PRIMARY SYSTEM FAILURE ~ FT²