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BEFORE THE UNITED STATES
ATOMIC ENERGY COMMISSION

In the Matter of	)	
Consolidated Edison Company of New York, Inc. (Indian Point Station, Unit No. 2)	)	Docket No. 50-247

ADDITIONAL TESTIMONY OF APPLICANT CONCERNING EMERGENCY CORE COOLING SYSTEM PERFORMANCE

July 13, 1971

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This testimony consists of a report on the emergency core cooling system performance of Indian Point Unit No. 2, with emphasis on the manner in which the AEC interim policy statement concerning emergency core cooling systems, published on June 29, 1971, are complied with. This report also contains the information requested by the Division of Reactor Licensing (DRL) in a letter of July 7, 1971. Appendix C provides references to the body of the report where responses to specific items are found and provides additional information requested by DRL not covered in the body of the report.

INDIAN POINT UNIT NO. 2

EMERGENCY CORE COOLING PERFORMANCE

This report covers the analytical techniques described in the topical report "Westinghouse PWR Core Behavior Following a Loss-of-Coolant Accident," WCAP-7422-L, January 1970 (Proprietary); the supplementary proprietary Westinghouse report "Emergency Core Cooling Performance," dated June 1, 1971; and includes the additional assumptions identified in Appendix A of the AEC Interim Policy Statement, "Criteria for Emergency Core Cooling Systems for Light Water Power Reactors," published in the Federal Register June 29, 1971, Vol. 36, p. 12247.

The acceptance criteria found in the Interim Policy Statement are addressed on page 2 of the attached report and results showing compliance with these limits are given on page 18. The additional assumptions identified in Appendix A are incorporated in the text describing the calculational procedure. These assumptions are repeated below and the pages noted where each is discussed.

1. The break discharge coefficient, (C_D), used with the Moody discharge flow model should be equal to 1.0 for all break sizes. Page 8
2. The decay heat curve described in the proposed ANS Standard, with a 20% allowance for uncertainty, should be used. The fraction of decay heat generated in the hot rod may be considered to be 95% of this value. Page 34

3. For large breaks in the range 0.6 to 1.0 times the total area of the double-ended break of the largest cold-leg pipe, two break models should be used. The first model should be the double-ended severance ("Guillotine"), which assumes that there is break flow from both ends of the broken pipe, but no communication between the broken ends. The second model should assume discharge from a single node ("split"). Page 18
4. The time after the break for the onset of departure nucleate boiling at the hot spot should be equal to 0.1 second. Page 24
5. For cold leg breaks, all of the water injected by the accumulators prior to end-of-blowdown shall be assumed to be lost. In this context the end-of-blowdown shall be specified as the time at which zero break flow is first computed. The containment back pressure assumed for the blowdown analysis should not be higher than the initial pre-break pressure plus 90% of the increase in pressure calculated for the accident under consideration. Page 9, 10
6. The pump resistance, K, used for analysis should be fully justified. The effect of pump speed upon K should be considered. The more conservative of two assumptions (locked or running) should be used for the pump during the blowdown calculation. Page 18
7. A calculation for the reflooding heat transfer should be performed. The containment back pressure assumed for the analysis should not be higher than the initial pre-break pressure plus 80% of the increase in pressure calculated for the accident under consideration. Page 13

The following items should be constraints on the calculation:

a. No steam flow should be permitted in intact loops during the time period that accumulators are injecting. Page 11

b. Core exit quality should be calculated from entering mass flow rate and nominal FLECHT heat transfer. Page 13

c. Pump resistance should be calculated on the basis of a locked rotor. Page 12

d. The effects of the nitrogen gas in the accumulator, which is discharged following accumulator water discharge, should be taken into account in calculating steam flow as a function of time. Page 14

e. The pressure drop in the steam generator should be calculated with the existing fluid conditions and associated loss coefficients. Page 11

f. All effects of cold injection water, in either a hot or cold leg, on steam flow (and ΔP) should be included in the calculation. Page 13

g. The heat transfer coefficient during reflood should be derived from FLECHT data. Page 30

General

A LOCA would result from a rupture of the Reactor Coolant System (RCS) or of any line connected to that system up to the first closed valve. The charging pumps have the capability to make up for leakages resulting from ruptures of a small cross section, thus permitting an orderly shutdown. A small quantity of the coolant containing fission products present in the coolant would be released to the containment.

For a postulated large break, reactor trip is initiated when the pressurizer low pressure set point is reached while the Safety Injection System (SIS) signal is actuated by coincident pressurizer low pressure and low level. The reactor trip and SIS actuation are also initiated by a high containment pressure signal. The consequences of the accident are limited in two ways:

1. Reactor trip and borated water injection supplement void formation in causing rapid reduction of the nuclear power to a residual level corresponding to the delayed fission and fission product decay.

2. Injection of borated water ensures sufficient flooding of the core to prevent excessive temperatures.

Before the reactor trip occurs, the plant is in an equilibrium condition, i.e., the heat generated in the core is being removed via the secondary system. After reactor trip and turbine trip, core heat, heat from hot internals

and the vessel is transferred to the RCS fluid, and then to the secondary system. The secondary system pressure increases until steam dump to the condenser occurs. Without offsite power,condenser cooling is lost and the power operated atmospheric relief valves and the safety valves open to the dump steam to the atmosphere. Make-up to the secondary side is automatically provided by the auxiliary feedwater pumps. The SIS signal stops normal feedwater flow by closing the main feedwater line isolation valves and initiates auxiliary feedwater flow by starting the motor driven auxiliary feedwater pumps. The secondary flow aids in the reduction of RCS pressure. When the RCS pressure falls below 600 psia, the accumulators begin to inject borated water.

Acceptance Criteria

The reactor is designed to withstand thermal effects caused by a LOCA including the double ended severance of the largest RCS pipe. The reactor core and internals together with the ECCS are designed so that the reactor can be safely shut down and the essential heat transfer geometry of the core preserved following the accident.

The ECCS, even when operating at partial effectiveness due to loss of one vital power bus (limiting case without off-site power), is designed to

limit the cladding temperature to well below the melting temperature of Zircaloy-4 and below the temperature at which gross core geometry distortion, including clad fragmentation, can be expected. In addition, the core metal-water reaction is limited to less than 1% of the available Zircaloy.

In order to assure effective cooling of the core, limits on peak clad temperature and local metal-water reaction have been established. It has been demonstrated in the single rod burst test phase of the Westinghouse Rod Burst Program,(1) that for conditions within the area of safe operation, as shown in Figure 1, fuel rod integrity is maintained. Additional experimental data could further increase this area of safe operation. Results of the multi-rod burst test phase of the Westinghouse Rod Burst Program(2) show that peak clad temperature calculated during the accident increases less than 100°F due to geometry distortion; thus peak clad temperatures determined on the basis of no geometry distortion should be limited to 100°F below the limits presented in Figure 1. However, in order to increase the safety margin, the peak clad temperature calculated without geometry distortion will be limited to 2300°F.

Environmental consequences of the ruptures are well within the limits of 10CFR100 guidelines.

THERMAL ANALYSIS

Method of Analysis

This analysis is performed by considering the following aspects of the accident: blowdown hydraulics, reactor kinetics, and the core cooling.

a) Blowdown Hydraulics. This calculation provides a description of the hydraulic response to a rupture of the RCS through completion of depressurization and the operation of the safety injection systems. The basic information concerning the dynamic behavior of the reactor core environment is thus provided for use in reactor kinetics and core cooling analyses. The code used in this part of the analysis is SATAN-V.

b) Reactor Kinetics. The core power transient is forced by the blowdown dynamics which in turn affects the blowdown. The kinetic calculation determines the energy added to the core which forms an essential input to the core cooling analysis. The SATAN-V code includes reactor kinetics calculation.

c) Core Cooling. Based on the above information, a detailed analysis of reactor core cooling is performed. The code used in this part of the analysis is LOCTA-R2 for fuel pellet temperature, cladding temperature, and metal-water reaction evaluations.

Blowdown Hydraulics (SATAN-V)

The blowdown hydraulic analysis for large size ruptures is required to provide the basic information concerning the dynamic behavior of the reactor core environment for the reactor kinetics and core cooling analysis. This requires the ability to predict the flow, quality, and pressure of the fluid passing through the core region. The SATAN-V code was developed with a capability to provide this information.

In a SATAN-V analysis the RCS is divided into a finite number of control volumes. The control volumes in the loop are divided into broken and unbroken loops and two control volumes are used to describe the coolant in the core region. This analysis replaces the FLASH-R analysis which had a limitation in that only three control volumes were available. The current multi-control volume analysis, particularly with two control volumes in the core, permits execution of a detailed parametric survey of the important phenomena affecting the blowdown process.

The adequacy of the SATAN code to predict the hydraulic behavior of the reactor coolant system during a loss-of-coolant accident has been verified by comparing the SATAN results of loss-of-coolant accidents of various sizes and locations performed at the LOFT and CSE facilities.[12]

In selecting the control volumes for the SATAN code, stability requirements dictate control volume selection of equal volume and length. In order to provide detail of the core region two core control volumes were selected.

This enables the flow at the top, middle, and bottom of the core to be calculated simultaneously. A total of 71 control volumes overall were selected for this analysis. Studies were performed to determine the effect of using more and less control volumes. The studies for less control volumes (15-20) showed that in addition to not having available the detail necessary to assess the flow regimes in the reactor vessel and core regions, significant changes in the answers resulted from small modeling changes. On the other hand a study was performed using 10 rather than 2 control volumes in the core resulted in only an insignificant change in the results. The current control volume selection is therefore felt to be adequate.

Later in this section a discussion is presented which shows the methods used to establish a conservative use of all the main assumptions used in the analysis. This study was performed to establish the conservative upper limit of the "confidence band" and forms the basis for this analysis.

Description of SATAN-V Code

The SATAN-V[4] (System Accident and Transient Analysis of Nuclear plants) computer code performs a comprehensive space-time dependent analysis of a LOCA and is designed to treat all phases of the accident. The three major phases are:

a) A subcooled phase where the rapidly changing pressure gradients in the subcooled fluid exert an influence upon the RCS internals and support structures.

b) A two-phase depressurization phase.

c) A refill-phase where the system pressure approaches the containment pressure.

The code employs a one-dimensional lumped parameter approach in which the entire reactor coolant loop system is divided into control volumes. The fluid properties are considered uniform and thermodynamic equilibrium is assumed in each element. Pump characteristics, pump coast-down and cavitation, core and steam generator heat transfer including the W-3 DNB correlation in addition to the reactor kinetics are incorporated in the code.

The reactor kinetics considered in the code is a point kinetics model identical to the CHIC-KIN[9] model. The reactor kinetics equations are the point kinetics equations with appropriately weighted feedback effects. The major reactivity feedback mechanisms incorporated in the kinetics equations are the moderator density change, moderator temperature change, Doppler broadening as well as the control rod motion. These reactivity forcing functions are inputs in tabular form as functions of time. A block diagram of this continuous feedback model is shown in Figure 2.

Fundamental Equations

The fundamental equations of conservation of mass, energy, and momentum are applied for single and two phase system in order to determine the main dependent variables: density and internal energy. A set of

auxiliary dependent variables is defined by the system governing algebraic equations. A critical flow calculation for subcooled, two phase, or superheated break flow is incorporated in the analyses.

The subcooled break flow is calculated using a modified version of the Zaloudek correlation.(5) The results are consistent with test results and do not produce a discontinuity between the subcooled and two-phase break flows. The two-phase break flow is calculated by using the Moody(6) two-phase correlation. Comparison of these correlations with experiments indicate that the break area must be reduced by a discharge coefficient to match the experimental break flow and depressurization rates. However, in all the design calculations, a break discharge coefficient of 1.0 is used.

Geometry

The SATAN-V Code is equipped to analyze a maximum of two loops plus "special" elements such as the reactor core, pressurizer and accumulators. This means that there is allowance for no more than two reactor coolant pumps and two steam generators. The unbroken loops are grouped together with proper scaling.

Method of Solution

The RCS is divided into a finite number of elements depending on the resolution desired and the computer is provided with the folliwng information:

a) Description of the way in which the elements are interconnected. Each

element can have as many as two flows out and up to five flows in, plus a break flow out.

b) Initial thermodynamic data for each element: enthalpy, pressure, mass flow rates and heat transfer characteristics.

c) Physical data for each element: dimensions and flow area, heat transfer area, length, volumes and friction characteristics.

Starting from a set of initial conditions the program evaluates the updated values of the dependent variables for all system elements using an integration routine based on the Taylor series. The computed updated values are then substituted as the initial conditions for the next integration cycle and the procedure repeated until the final problem time is reached.

Description of the Core Reflooding Model

The SATAN calculations are performed until the completion of blowdown. In this context the end of blowdown is defined as the time at which zero break flow is first computed. The containment break pressure assumed for the blowdown analyses is equal to the initial pre-break pressure plus 90% of the increase in pressure calculated for the accident analyzed. At this time, the normal blowdown transient calculations are terminated and the reflooding calculations are performed. The reflooding model consists of three reference volumes which represent the downcomer region, the lower plenum region, and the active core region. The core and the downcomer

volumes both communicate with the lower plenum volume via non-resistive flow paths. An input containment back-pressure is assumed to act directly on the top of the downcomer volume, and any steam generated in the core region is vented to the containment via a flow path whose resistance simulates the flow path to the break. The model is shown in Figure 3.

At the end of blowdown, the lower plenum volume is assumed to contain all the water remaining in the downcomer region below the nozzle and in the lower plenum. The water injected by the accumulator prior to the end of blowdown is conservatively assumed to be lost. No credit is taken for water remaining in any other control volume. The lost accumulator water delays the reflooding, extends the period of core recovery and results in a longer adiabatic core heat-up. Accumulator flow and cold-leg safety injection flow are continuously calculated as they were during the blowdown transient and are added to the downcomer region. Until the water reaches the bottom of the core, the water levels in the downcomer and in the region below the core are equal. When the water level in the downcomer region reaches the bottom of the inlet nozzle, water from the injection flow is assumed to spill through the break into the containment sump. Provisions for heat transfer from vessel walls and reactor internals to injection water are also included in this model.

When the bottom of the core is reflooded by the accumulator water, steam is generated by the hot fuel rods, causing a pressure build-up in the core region. This retards the core reflooding process. The steam

generated must be vented from the system through the break, and the flooding rate is limited by the resistance of the loop to the steam and water flow. There are two paths available for the steam and entrained water to flow to the break. The first path is directly to the break through the broken loop. The other path is through the intact loops, back into the inlet annulus, and finally to the break through the inlet nozzle in the broken leg. These flow paths are illustrated in Figure 4 and 5. The sketches in Figures 4 and 5 show the path the steam must follow for the cold leg break. (Note that figure 5 shows one intact loop and one broken loop for the purpose of illustration.) The pressure drops along these two paths are calculated with the existing fluid conditions and associated loss coefficients. The pressure drop across the pump is maximized by assuming that the rotor is locked. In addition, it is postulated that the accumulator water injected in the cold leg pipe completely fills the pipe, thus forming a plug that prevents venting of the steam generated in the core during reflooding. These assumptions tend to reduce the core flooding rate and the fuel rod heat transfer, thus resulting in increased peak clad temperature.

The amount of mass evaporized and entrained as a function of core flooding rate and time after reflooding is obtained from an analysis of the FLECHT[7][8] results. These results indicate that several flow regimes are present in the rod bundle during reflooding. For the first few seconds of the reflooding transient, until the core floods to approximately 20 inches, most of the heat transferred from the rod to the coolant goes to increase the liquid enthalpy. During this period almost no steam generation

takes place and the core flooding rate equals the vessel cold flooding rate. Following this initial period the steam velocity increases above the value required for entrainment and a dispersed flow regime begins. This flow pattern is characterized by a continuous vapor phase with dispersed droplets and by a fast increase in rod heat transfer coefficient.

It is during this phase of the reflooding transient that the flooding rate into the core is reduced by the resistance of the flow paths from the core to the break. The core flooding rate transient during this period is a function of the core and loop resistance, the fraction of coolant evaporized and entrained, and the difference in water level between the downcomer and the core. The fraction of coolant evaporized, entrained and leaving the core is not constant during the transient, but increases from zero at the beginning to 0.7 - 0.8 several seconds after initiation of reflooding depending on the core flooding rate. At the same time, due to the reduction in core flow, the water level in the downcomer region increases at a faster rate thus providing the water head required to discharge the increased exit core flow to the break.

In the reflooding calculations in addition to assuming a locked rotor pump and the cold leg pipe plugged during accumulator injection, the following assumptions are made. Each of these assumptions is conservative because they result in increased venting path pressure drop and therefore lower flooding rate.

1. The fraction of coolant evaporized and leaving the core is assumed to be equal to 0.8 for flooding rates up to 2 in/sec and increases to 0.85 for flooding rates higher than 4 in/sec.

2. No transient effects are considered in the transition between high to low flooding rates, but, even during this period of time, the core flooding rate is calculated by assuming that the fraction of core inlet flow that has to be vented to the break is equilibrium values specified in No. 1 above.

3. No water-steam separation is assumed to occur in the upper plenum and the quality of the mixture entering the loops is calculated from the core mass flow rate and nominal FLECHT heat transfer.

4. All the coolant is assumed to be superheated to 500°F in the steam generators. The superheating of the steam is conservatively assumed to take place instantaneously at the steam generator inlet.

5. The containment back pressure is equal to the initial pre-break pressure plus 80% of the calculated pressure increase for the accident.

6. For the steam velocities occurring in the cold leg pipe and mass flow rate of the safety injection system, the acceleration pressure loss of accelerating all the water to the steam velocity is balanced by a small reduction in the steam temperature. Therefore, the injection of SIS water in the cold leg does not increase the loop pressure losses during reflooding.

7. The beneficial effects of nitrogen discharging after the end of blowdown are not taken into account during the reflooding time period. Since the nitrogen discharges into the cold leg, the downcomer will be pressurized a very short time period before the hot leg. This will result in an additional amount of downcomer water entering the core that is not accounted for in the reflood analysis.

Core Cooling - LOCTA-R2 Code

The LOCTA-R2 transient digital computer program was developed for evaluation of fuel pellet and cladding temperature during a LOCA. It also determines the extent of the Zircaloy-steam reaction and magnitude of the resulting energy release in Zircaloy clad cores.

The transient heat conduction equation is solved by means of finite differences considering only heat flow in the radial direction. A lumped parameter method is used; the fuel containing three radial nodes and the cladding one node with a specified resistance simulating the pellet to clad gap.

Internal heat generation can be specified as a function of time. The decay heat is based on the heat generated from:

a) fission products;

b) capture products; and

c) residual fissions.

It is assumed that the core has been irradiated for an infinite period of time. The method used to calculate the total decay heat is presented in Appendix B.

In addition to decay heat the code calculates the heat generated due to the Zircaloy-steam reaction. The Zr-H_2O reaction is governed by the parabolic rate equation[13][14] unless there is an insufficient supply of steam available, then a "steam limited" evaluation is made. In the design calculations the parabolic rate equation is assumed for the full transient even when the fuel rod is assumed adiabatic. The buildup of the Zircaloy-oxide film is calculated as a function of time, and its effect on heat transfer is considered. An isothermal clad melt is considered based on the heat of fusion of Zircaloy.

The code has been developed to stack axial sections and thereby describe the behavior of a full length region as a function of time. A mass and energy balance is used to evaluate a temperature rise in the steam as it flows through the core. Each radial region is considered independently and a number of axial sections which may be analyzed is essentially unlimited.

The initial conditions for the fuel rod are specified as a function of power. The following core conditions are also introduced as a function of time:

a) mass flow rate through the core;

b) coolant quality;

c) pressure.

To assure the conservatism of the hot channel temperature calculations the following procedure is used for the hot spot temperature calculation:

1. The higher of the two SATAN core control volume quantities is used in hot spot temperature calculations.

2. The flow used in the hot channel temperature calculations is the core midplane SATAN flow reduced by 20%.

Blowdown and reflooding heat transfer processes in the core are evaluated by means of using correlations summarized in Appendix A which have been validated with several Westinghouse Research and Development Programs. From the end of blowdown until core reflood the core is assumed adiabatic even though steam cooling is expected because of the local depressurization caused by the accumulators and by circulation promoted by the core and other heat sources. In summary, information generated by LOCTA-R2 as a function of time includes:

a) fuel temperature;

b) clad temperature;

c) steam temperature;

d) amount of metal-water reaction, if any;

e) volume of core melt, if any;

f) total heat released to coolant.

A more detailed explanation of the models and correlations used in LOCTA-R2 is presented in WCAP-7437-L[10].

Results

The detailed description of the Reactor Coolant System that can be obtained in the multi-control volumes SATAN-V code has been used to analyze important phenomena affecting the blowdown process, such as:

1. Heat transfer from core to the coolant during blowdown
2. Reactor Coolant Pump Characteristics
3. Steam Generator Heat Transfer Characteristics
4. Loop Resistances and Break Location
5. Accumulator Performance

A parametric survey study was performed for the Indian Point Unit No. 2 Final Safety Analysis Report,[11] Supplements 12 and 13 with the purpose of determining the most conservative combination of the above assumptions as input to the SATAN-V code. Additional parametric studies performed for Turkey Point Unit No. 2 and Indian Point Unit No. 2 are presented in the 6/1/71 AEC submittal "Emergency Core Cooling Performance"[3] The results presented in this section are based on the conservative assumptions determined in that study. These conservative assumptions were, for the limiting break condition (Cold Leg Double-Ended Guillotine Break), that the pump was assumed to trip at the time of the break and continued to coast down until cavitation conditions were reached (at this time the pump was assumed to not lock but continued to develop a conservative head). The pump speed is continually calculated as a function of prevailing conditions and the pump characteristics.

The analysis of the LOCA was performed at 102% of the maximum calculated power of 2758 MWt and at a peak linear power of 17.4 kw/ft. This value of peak linear power includes a 5% allowance for nuclear uncertainties.

Table 1 presents the results of the loss of coolant accident analyses for a range of break sizes.

Table 2 presents the hot spot metal water reaction for each break.

Figures 6 through 9 present the results of the SATAN-V analysis in terms of core flow, quality, and pressure as a function of time after the accident. Figures 10 through 13 present clad temperature transients. Figures 14 through 17 present core pressure drop. Figures 18 through 21 present hot spot fluid temperature. Figures 22 through 25 present flow in the broken and intact hot-leg and cold-leg piping and figures 26 through 29 present flow in the upper plenum, lower plenum and flow out of the break.

The peak clad temperature occurred following the double-ended cold leg break and did not exceed a value of 2300°F. The hot spot metal water reaction is 7.5% which meets the design criteria. In addition, the total core metal water reaction is less than 1%.

Conclusions - Thermal Analysis

For breaks up to and including the double-ended severance of a reactor coolant pipe, the ECCS with partial effectiveness will limit the clad temperature to 2300°F and assure that the core will remain in place and substantially intact with its essential heat transfer geometry preserved. The ECCS design meets the core cooling criteria as previously stated.

TABLE 1

PEAK CLAD TEMPERATURES FOR
INDIAN POINT UNIT #2

Break	Peak Temperature
Double Ended	2300 °F
0.8 Double Ended	2280 °F
4.5 Ft^2	2160 °F
3.0 Ft^2	1715 °F

TABLE 2

INDIAN POINT UNIT #2

HOT SPOT CLAD METAL - WATER REACTION

BREAK	PERCENT ZIRC REACTED
Double Ended	7.5
0.8 Double Ended	7.2
4.5 Ft^2	5.1
3.0 Ft^2	0.0

REFERENCES

(1) "Topical Report - Performance of Zircaloy Clad Fuel Rods During a Simulated Loss-of-Coolant Accident - Single Rod Tests, "WCAP-7379-L, Volume I (Proprietary) and Volume II (Non-proprietary), September, 1969.

(2) "Topical Report - Performance of Zircaloy Clad Fuel Rods During a Simulated Loss-of-Coolant Accident - Multi-Rod Tests, "WCAP-7495-L, Proprietary, Volume I - Test setup and Results; Volume II - Analyses of Results, July 1969.

(3) "Emergency Core Cooling Performance" (W Proprietary) 6/1/71 AEC submitted.

(4) F. M. Bordelon, "A Comprehensive Space-Time Analysis of Loss-of-Coolant (SATAN Digital Code), WCAP-7263 (W Proprietary), March 1969.

(5) F. R. Zoloudek "Steam Water Critical Flow from High Pressure System," Hanford Laboratories, HW-80535, January 1964.

(6) F. H. Moody, "Maximum Flow Rate of Single Component, Two-Phase Mixture," Paper NO. 64-HT-35, and ASME Publication.

(7) J. O. Cermak, et al., "Full Length Emergency Cooling Heat Transfer (FLECHT) Group I Tests," WCAP-7435, January 1970.

(8) F. F. Cadek, et. al., "Full Length Emergency Cooling Heat Transfer (FLECHT) Group II Test Report," WCAP-7544, September, 1970.

(9) V. A. Redfield, "CHIC-KIN. . . A Fortran Program for Intermediate and Fast Transients in a Water Moderated Reactor, WAPD-TM-479 (January 1965).

(10) W. A. Bezella, et. al., "LOCTA-R2 Loss of Coolant Transient Analysis," WCAP-7437-L, January 1970.

(11) Supplement 12 to the IPP No. 2 FSAR, (Docket No.'s 50-247 and 50-286).

(12) "Topical Report - Westinghouse PWR Core behavior Following a Loss of Coolant Accident," WCAP-7422-L. (W Proprietary), January 1970.

(13) James C. Hesson, et. al., "Laboratory Simulations of Cladding - Steam Reactions Following Loss of Coolant Accidents in Water-Cooled Power Reactors" ANL-7609

(14) L. Baker, Jr. and Just, J. C., "Studies of Metal Water Reactions At High Temperature," ANL-6548, 1962

APPENDIX A

HEAT TRANSFER COEFFICIENTS USED IN THE LOCTA-R2 CORE THERMAL ANALYSES

The heat transfer correlations used in the LOCA analyses are presented below in order of application during the accident.

Time of break until occurrence of DNB

The time of the break until occurrence of DNB is taken conservatively to be 0.1 sec. The heat transfer regime during this period of the accident is forced convection turbulent heat transfer or fully developed nucleate boiling. The correlation for nucleate boiling is that by Jens and Lottes.[1]

$$T_W - T_{sat} = \Delta T_{sat} = 1.9\ e^{-p/900}\ (q'')^{0.25}$$

In the nucleate boiling regime the wall temperature is a function of the heat flux and pressure, not coolant velocity. The Jens and Lottes correlation is independent of geometry, i.e., valid for tubes, plates or rod bundles. It is also used for both local and bulk boiling. The correlation has been compared to subcooled water data obtained from single heated tubes having internal diameters from 0.143" to 0.288", length from 3" to 24.6" and pressure ranging from 500 to 2000 psia. The Dittus-Boelter correlation is used for forced convection turbulent heat transfer.

From DNB until time of uncovering (steam cooling period).

In the large break design calculations DNB is assumed to occur at 0.1 sec. After DNB occurs, the mode of heat transfer is unstable with both nucleate boiling and film boiling existing from times of short duration. The heat transfer correlation used in the transition to film boiling period is described in WCAP-9005[2] and the 6/1/71 AEC submittal, "Emergency Core

Cooling Performance." The verification of the use of this correlation for post DNB heat transfer during a transient blowdown is also presented.

The initial operating conditions prior to blowdown are:

1. Pressure	2250 psia
2. Mass Velocity	1,000,000 to 2,500,000 lb/hr ft^2
3. Inlet Temperature	480 to 560°F
4. Heat Flux	635,000 to 1,100,000 Btu/hr ft^2

Blowdown conditions were:

1. Initial blowdown rate	200 to 10,000 psi/sec
2. Average flow decay rate	250,000 to 1,150,000 lb/hr ft^2 sec

The test section consisted of a 1/2 inch inside diameter circular tube 3 feet long. The 3 foot length is sufficient to establish fully developed flow at the exit of the test section (L/D = 72).

A total of 50 transient blowdown runs were performed. To determine the effect of flow decay 20 runs were performed during which the flow rate was maintained as close as possible to the initial value. Thirty runs were performed in which the flow to the test section was allowed to decay in addition to the depressurization. This latter condition more nearly resembles the predicted conditions in a PWR core during the large break LOCA. For this reason the majority of the runs were performed with flow decay.

The comparison of predicted heat transfer coefficient with the measured data is shown in Figure A-1 for all data points. It is readily apparent that the correlation is conservative with respect to the results of this

test since the measured value is greater than predicted for 95% of the data. The degree of conservatism contained in the correlation increases with increasing values of the heat transfer coefficient.

During uncovering (steam cooling period)

This period of the loss-of-coolant accident considers either turbulent or laminar forced convection to steam combined with radiation from the fuel rod surface to the steam. Radiation between fuel rods is not considered.

a) For turbulent forced convection to steam a Dittus-Boelter type equation, modified by McEligot[3] [4] to account for the variation in fluid properties near the wall due to a large temperature gradient, is used.

$$N_u = 0.020\ (Re)_b^{0.8}\ (Pr)_b^{0.4}\ \left(\frac{Tw}{Tb}\right)^{-0.5}$$

The $\left(\frac{Tw}{Tb}\right)$ term is independent of geometry. The Dittus-Boelter equation was developed from flow inside tubes with values of C=0.023. Weisman[5] has shown that C is higher for rod bundle data (~0.025). A lower value of C as shown in the above correlation is presently used in the loss-of-coolant analyses.

The McEligot correlation was compared to data with hydraulic equivalent diameter values of 0.125" and 0.25" and L/D greater than 150. Additional coolant conditions are described below.

Coolant	Reynolds Number	Maximum Wall Temperature, °F	Tw/Tb
Air	1450-15000	1520	2.17
Helium	7570-13400	1050	1.56
Nitorgen	18200-45000	1620	4.78

The Prandtl number of steam is similar to those obtained with the above coolants.

b) For laminar forced convection to steam, the heat transfer correlation used is based on theoretical calculations of laminar flow in tubes made by Hausen[6] and Kays[7].

$$\left(\frac{hD_e}{k}\right)_{iso} = 3.66$$

$$h/h_{iso} = \left(\frac{Tw}{Tb}\right)^{-0.25}$$

These calculations indicate that the local Nusselt number is highest near the inlet and drops until it reaches a limit corresponding to fully-developed thermal conditions. For the case of constant wall temperature, the limiting Nusselt number is 3.66. For constant heat flux at the wall, the asymptote is 4.36.

Furthermore, these calculations indicate that the asymptotic values are reached for all practical purposes when $L/D/RePr > 0.05$. For the Reynolds numbers ranging from 100 to 1000 and a $Pr = 1.0$ for steam, the developing length is from 2.5" to 25".

The correlation was compared to data from laminar air flow in circular tubes where the L/D ranged from 42 to 80 and $Re < 3000$.

The $\left(\frac{T_w}{T_b}\right)^{-0.25}$ term is to account for variations in fluid properties near the wall due to large temperature gradients.

c) Radiation to steam is evaluated employing the emperical method of Hottel.[8]

$$h = 0.1713 \times \varepsilon \frac{\left[\left(\frac{T_w}{100}\right)^4 - \left(\frac{T_{H_2O}}{100}\right)^4\right]}{T_w - T_b}$$

where

$$\varepsilon = \frac{1}{\frac{1}{\varepsilon_W} + \frac{1}{\alpha_{H_2O}} - 1}$$

$$\alpha_{H_2O} = \varepsilon' \; H_2O \; \left(\frac{T_{H_2O}}{Tw}\right)^{0.45} C_{H_2O}$$

The present value of the correction factor, C, for ε_{H_2O} at higher pressure than 0 or 1 atmosphere is 2.0.

Verification of Correlations Used During Steam Cooling Period

The use of the above correlations during the steam cooling period was verified by the work performed at the University of Michigan under Westinghouse funding and direction. This was part of the Flashing Heat Transfer Program. The results of this phase of the program have been documented in WCAP-7396-L[9]. The primary objective of this test was to determine the behavior of radiation heat transfer to steam at elevated pressures (up to 5 atm.).

The heat transfer test facility consisted of an open heat transfer loop. Steam was delivered to the test section and discharged to the atmosphere through the necessary piping and control apparatus.

The test sections consisted of 1/2 inch ID pipes, with an active length of three feet. The walls of the test section were heated by electrical resistance heating. Test sections having both uniform and non-uniform axial heat generation were employed.

The range of variables were representative of that in a PWR when the core is uncovered and is as follows:

Mass Velocity	$G = 4 \times 10^3$ to 4×10^4 lb/hr-ft^2
Temperature	T_{in} = 300 to 100°F
Wall Temperature	400 to 1800°F
Pressure	P_{in} = 25 to 75 psia
Inlet Reynolds Number	1900 to 35000

The results of the low pressure heat transfer test yield the following conclusions:

1. The McEligot et. al. correlation realistically predicts the convective heat transfer coefficients in turbulent flow.

2. In turbulent flow the radiant heat transfer contribution to the total heat transfer coefficient is adequately predicted by Hottel's technique.

3. The total heat transfer coefficient to steam in turbulent flow may be calculated by adding the convective term determined by McEligot's correlation and radiative term determined by Hottel's technique. A comparison of predicted versus measured total turbulent heat transfer coefficient is shown in Figure A-2 and excellent agreement can be seen.

4. In laminar flow the total heat transfer coefficient is conservatively predicted by using the correlation of Hausen and Kays for the convective contribution and the method of Hottel for the radiant contribution.

5. The prediction of laminar heat transfer coefficient can be improved by evaluating the steam properties at film conditions instead of bulk conditions.

6. The effect of a non-uniform heat flux on the heat transfer coefficient is negligible for the conditions which exist during a LOCA.

Recovery phase of the accident

After entrainment has been initiated, heat transfer coefficients obtained from the FLECHT Program[10] are used. Detailed results of the Group I and Group II test series can be found in Reference 11 and 12, respectively. In summary, the current test results verified the ability of a bottom flooding SIS design to terminate the temperature increase during a LOCA. In particular, it has been shown that the effects of a variable flooding rate can be predicted, using constant flooding rate data and that complete blockage of as many as sixteen adjacent channels will not impair bottom flooding core cooling effectiveness.

NOMENCLATURE

SYMBOLS

D_e - equivalent diameter

G - mass velocity

L - length of heat source

N_u - Nusselt number

P - system pressure

Pr - Prandtl number

Re - Reynolds number

T - temperature

g - gravity constant

h - heat transfer coefficient

k - thermal conductivity

q" - heat flux

ε - effective emissivity

μ - dynamic viscosity

ρ - density

SUBSCRIPTS

b - quantities evaluated at bulk fluid temperature

iso - evaluation of the parameter when the temperature difference $(T_w - T_b)$ is small

sat - refers to saturated condition

v - saturated vapor

L - refers to liquid

w - wall

REFERENCES

1. W. H. Jens and P. A. Lottes, "Analyses of Heat Transfer, Burnout, Pressure Drop, and Density Data for High Pressure Water," USAEC Report ANL-4627, 1951.

2. R. F. Farman and J. O. Cermak, "Post DNB Heat Transfer During Blowdown," WCAP-9005, October 1968, Proprietary.

3. D. M. McEligot, P. M. Magee, and G. Leppert, "Effect of Large Temperature Gradients on Convective Heat Transfer: The Downstream Region," J. of Heat Transfer, Vol. 87, 1965, pp. 67-76.

4. D. M. McEligot, L. W. Ormand, and H. C. Perkins, "Internal Low Reynolds - Number Turbulent and Transitional Gas Flow with Heat Transfer," J. of Heat Transfer, Vol. 88, 1966, pp. 239-245.

5. J. Weisman, "Heat Transfer to Water Flowing Parallel to Tube Bundles," Nuclear Science and Engineering 6, 79, 1969.

6. H. Hausen, "Darstellung des Warmeuberganges in Rohren durch verall gemeinerte Potenzbezienhungen," VDI Zeit., No. 4, p. 91, 1943.

7. W. M. Kays, "Numerical Solutions for Laminar-Flow Heat Transfer in Circular Tubes," Trans. ASME, Vol. 77, 1955, pp. 1265-2374.

8. H. C. Hottel, "Radiation Heat Transmission," Ch. 4 of Heat Transmission by W. H. McAdams, McGraw-Hill, 1954.

9. R. M. Hunt (Editor) "Safety Related Research and Development for Westinghouse Pressurized Water Reactors - A Program Outline Fall 1969," WCAP-7396-L, November 1969, Proprietary.

References (Cont.)

10. R. M. Hunt (Editor), "Safety Related Research and Development for Westinghouse Pressurized Water Reactors - A Program Outline Spring 1969," WCAP-7304-L, April 1969, Proprietary.

11. J. O. Cermak, et.al., "Full Length Emergency Cooling Heat Transfer (FLECHT) Group I Tests," WCAP-7435, January 1970.

12. F. F. Cadek, et.al., "Full Length Emergency Cooling Heat Transfer (FLECHT) Group II Test Report," WCAP-7544, September, 1970.

APPENDIX B

RESIDUAL DECAY HEAT

In the analysis of loss of coolant transients, the residual decay heat during the period immediately after shutdown is important in determining peak clad temperatures. The decay heat data used as input to these analyses is described in this section.

Residual heat in a subcritical core consists of (a) fission product decay energy, (b) decay of neutron capture products, and (c) residual fissions due to the effect of delayed neutrons. These constituents are discussed separately in the following paragraphs.

Fission Product Decay

For short times ($<10^3$ seconds) after shutdown, data on yields of short half-life isotopes is sparse. Very little experimental data is available for the γ-ray contributions and even less for the β-ray contribution. Several authors have compiled the available data into a conservative estimate of fission product decay energy for short times after shutdown, notably Shure[1], Dudziak[2], and Teage[3]. Of these three selections, Shure's curve is the highest, and it is based on the data of Stehn and Clancy[4] and Obenshain and Foderaro[5].

The fission product contribution to decay heat which has been assumed for the loss of coolant analyses in this application is the curve of Shure increased by 20% for conservatism. This curve with the 20% factor included is shown in Figure B-1. The curve of Shure coincides with the recommendation in the proposed ANS standard on fission product decay heat (unpublished). The proposed standard also recommends that 20% be added for conservative design.

Decay of U-238 Capture Products

Betas and gammas from the decay of U-239 (23.5 min. half-life) and Np-239 (2.35 day half-life) contribute significantly to the heat generation after shutdown. The cross-section for production of these isotopes and their decay schemes are relatively well known. For long irradiation times their contribution can be written as:

$$P_1/P_o = \frac{\gamma 1 + E_{\beta 1}}{200 \text{ Mev}} \; c(1+\alpha)\; e^{-\lambda_1 t} \text{ watts/watt}$$

$$P_2/P_o = \frac{E_{\gamma 2} + E_{\beta 2}}{200 \text{ Mev}} \; c(1+\alpha) \left[\frac{\lambda_2}{\lambda_1 - \lambda_2} \left(e^{-\lambda_2 t} - e^{-\lambda_1 t}\right) + e^{-\lambda_2 t} \right] \text{watts/watt}$$

where: P_1/P_o is the energy from U-239 decay

P_2/P_o is the energy from Np-239 decay

t is the time after shutdown (secs)

$c(1+\alpha)$ is the ratio of U-238 captures to total fissions = .6(1+.2)

λ_1 = the decay constant of U-239 = 4.91×10^{-4} secs^{-1}

λ_2 = the decay constant of Np-239 = 3.41×10^{-6} secs^{-1}

$E_{\gamma 1}$ = total γ-ray energy from U-239 decay = .074 MeV

$E_{\gamma 2}$ = total γ-ray energy from Np-239 decay = .30 MeV

$E_{\beta 1}$ = total β-ray energy from U-239 decay = 1/3 x 1.18 MeV

$E_{\beta 2}$ = total β-ray energy from Np-239 decay =1/3 x .43 MeV

(Two-thirds of the potential β-energy is assumed to escape by the accompanying neutrinos.)

This expression with a margin of 10% is shown in Figure B-1. The 10% margin, compared to 20% for fission product decay, is justified by the availability of the basic data required for this analysis. The constants given above are in agreement with the coefficients of the proposed ANS standard. The decay of other isotopes, produced by neutron reactions other than fission, is neglected.

Residual Fissions

The time dependence of residual fission power after shutdown depends on core properties throughout the transient. Spatially dependent kinetics calculations have not been performed for these transients. It is assumed that core average conditions are more conservative for the calculation of reactivity and power level than actual local conditions as they would exist in hot areas of the core. Thus, static power shapes are assumed (with the exception of the effect discussed in the next section) and these are factored by the time behavior of core average fission power calculated by a point model kinetics calculation with six delayed neutron groups.

For the purpose of illustration, only a one delayed neutron group calculation, with a constant reactivity of -4%$\Delta\rho$, is shown in Figure B-1.

Distribution of Decay Heat

During a loss of coolant accident the core is rapidly shut down by void formation or control rods, or both, and a large fraction of the heat generation to be considered comes from fission product decay gamma rays. This heat is not distributed in the same manner as steady state fission power. Local peaking effects which are important for the neutron dependent part of the heat generation do not apply to the gamma ray contribution. The steady state factor of 97.4% which represents the fraction of heat generated within the fuel, drops to 95% for the hot rod in a loss of coolant accident.

For example, consider the transient resulting from the double ended break of the largest primary circuit pipe; 1/2 second after the rupture about 30% of the heat generated in the fuel rods is from gamma-ray absorption. The gamma power shape is less peaked than the steady state fission power shape, reducing the energy deposited in the hot rod at the expense of adjacent colder rods. A conservative estimate of this effect is a reduction of 10% of the gamma-ray contribution of 3% of the total. Since the water density is considerably reduced at this time, an average of 98% of the available heat is deposited in the fuel rods, the remaining 2% being absorbed by water, thimbles, sleeves and grids. The net effect is a factor of .95 rather than .974, to be applied to the heat production in the hot rod.

References

1. K. Shure, "Fission Prod-ct Decay Energy," Bettis Technical Review, WAPD-BT-24, December, 1961.

2. K. Shure and D. J. Dudziak, "Calculating Energy Release by Fission Products," Trans. Am. Nucl. Soc. 4 (1) 30,1961.

3. U.K.A.E.A. Decay Heat Standard (Reference unavailable).

4. J. R. Stehn and E. F. Clancy, "Fission-Product Radioactivity and Heat Generation","Proceedings of the Second United Nations International Conference on the Peaceful Uses of Atomic Energy, Geneva, 1958,"Vol. 13, pp. 49-54, United Nations, Geneva, 1958.

5. F. E. Obenshain and A. H. Foderaro, "Energy from Fission Product Decay" WAPD-P-652, 1955.

PEAK CLAD TEMPERATURE AND METAL-WATER REACTION
FOR A LOSS OF COOLANT ACCIDENT

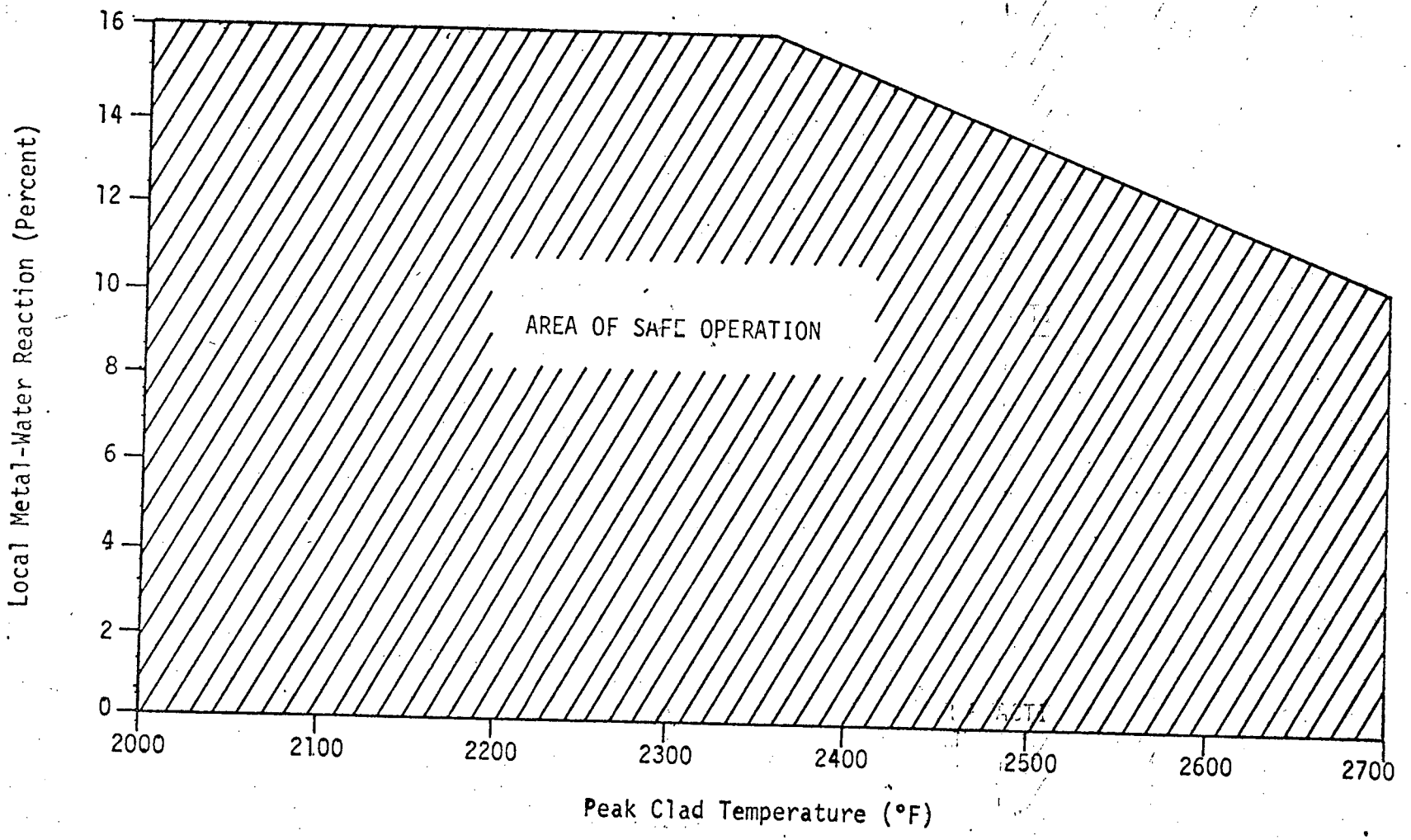


FIG. 1

A BLOCK DIAGRAM OF THE CONTINUOUS FEEDBACK MODEL CHIC-SIN CODE

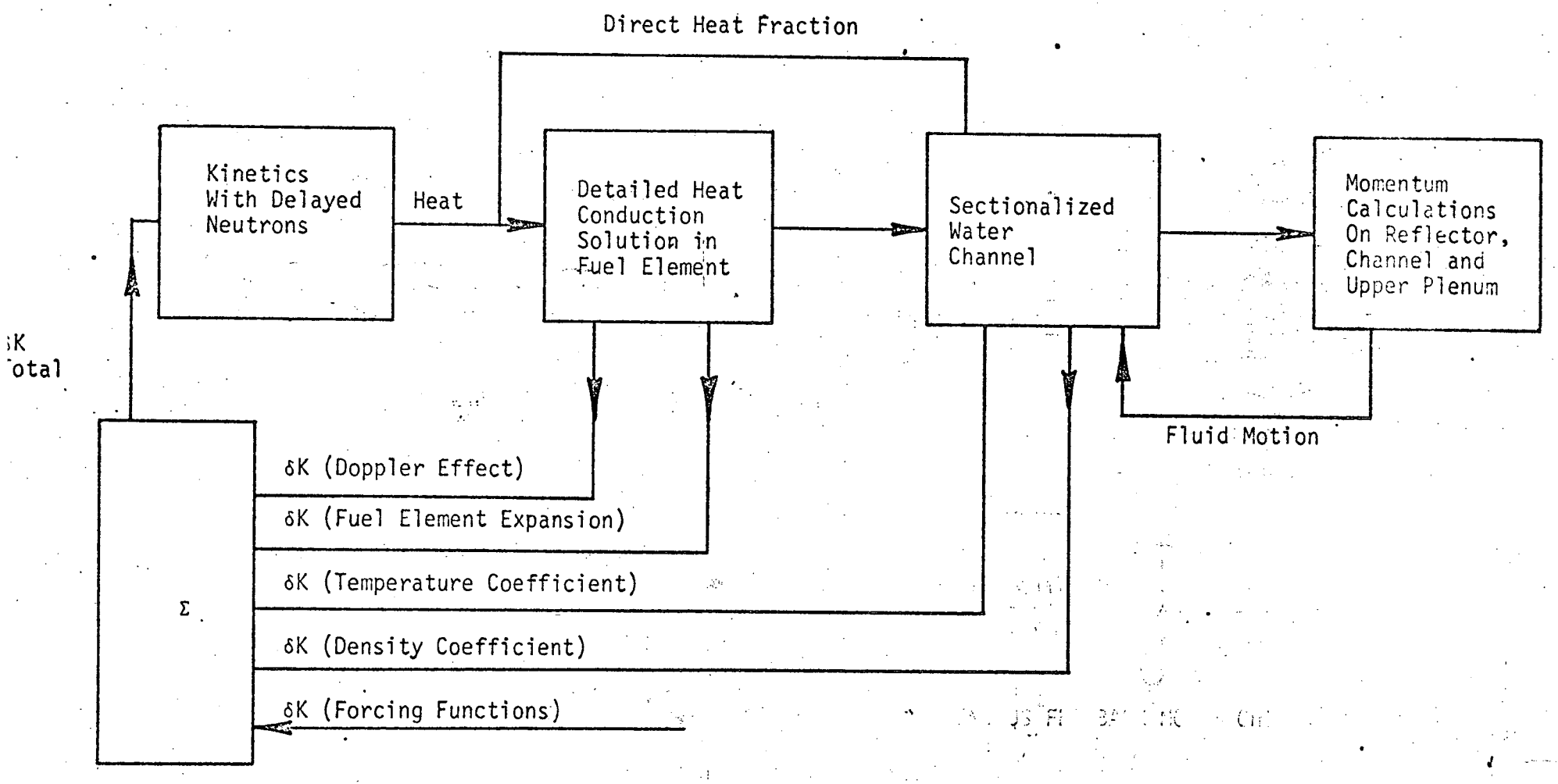
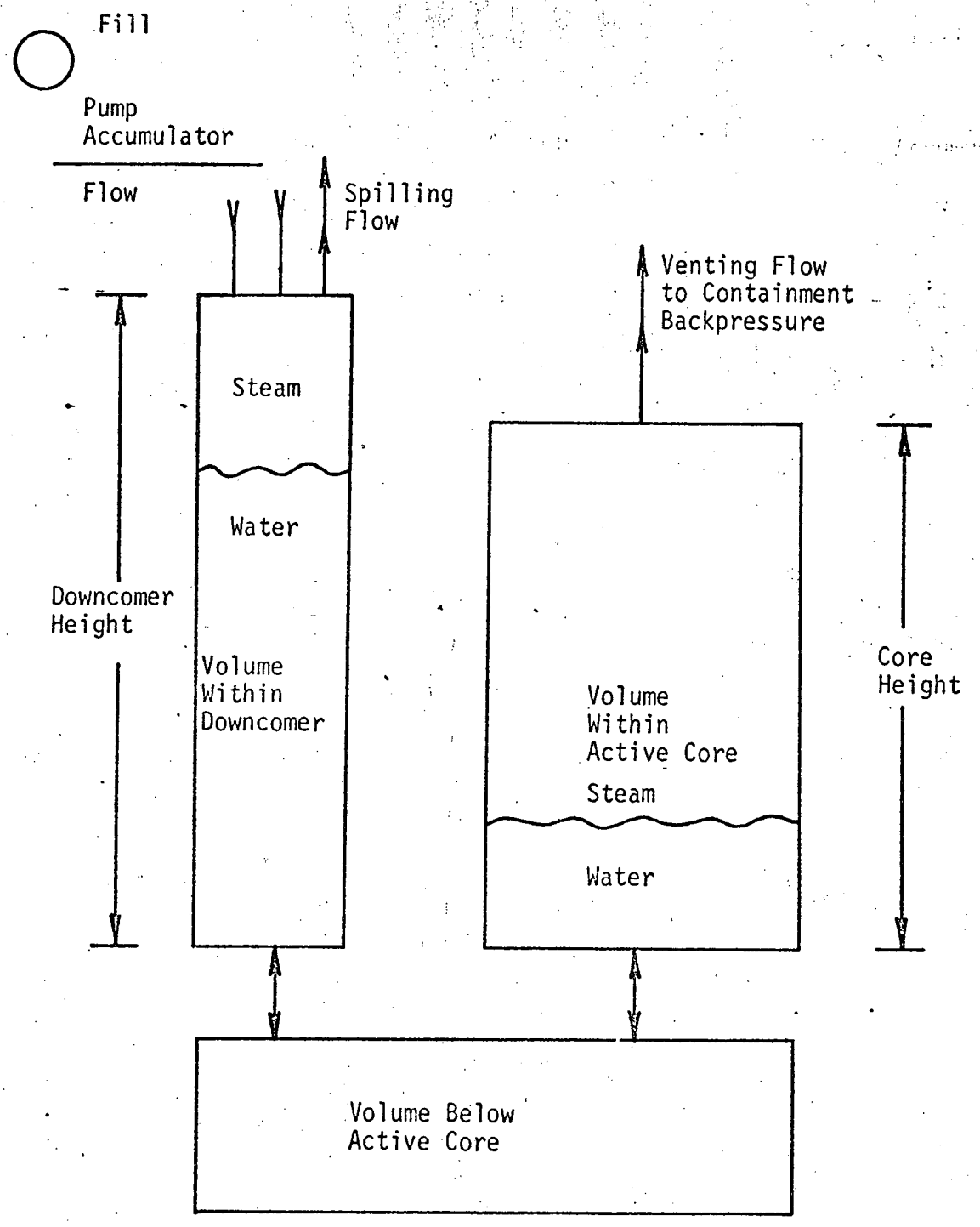


FIG. 7

FIG. 3

BLOCK DIAGRAM FOR SATAN-V REFILL CALCULATION



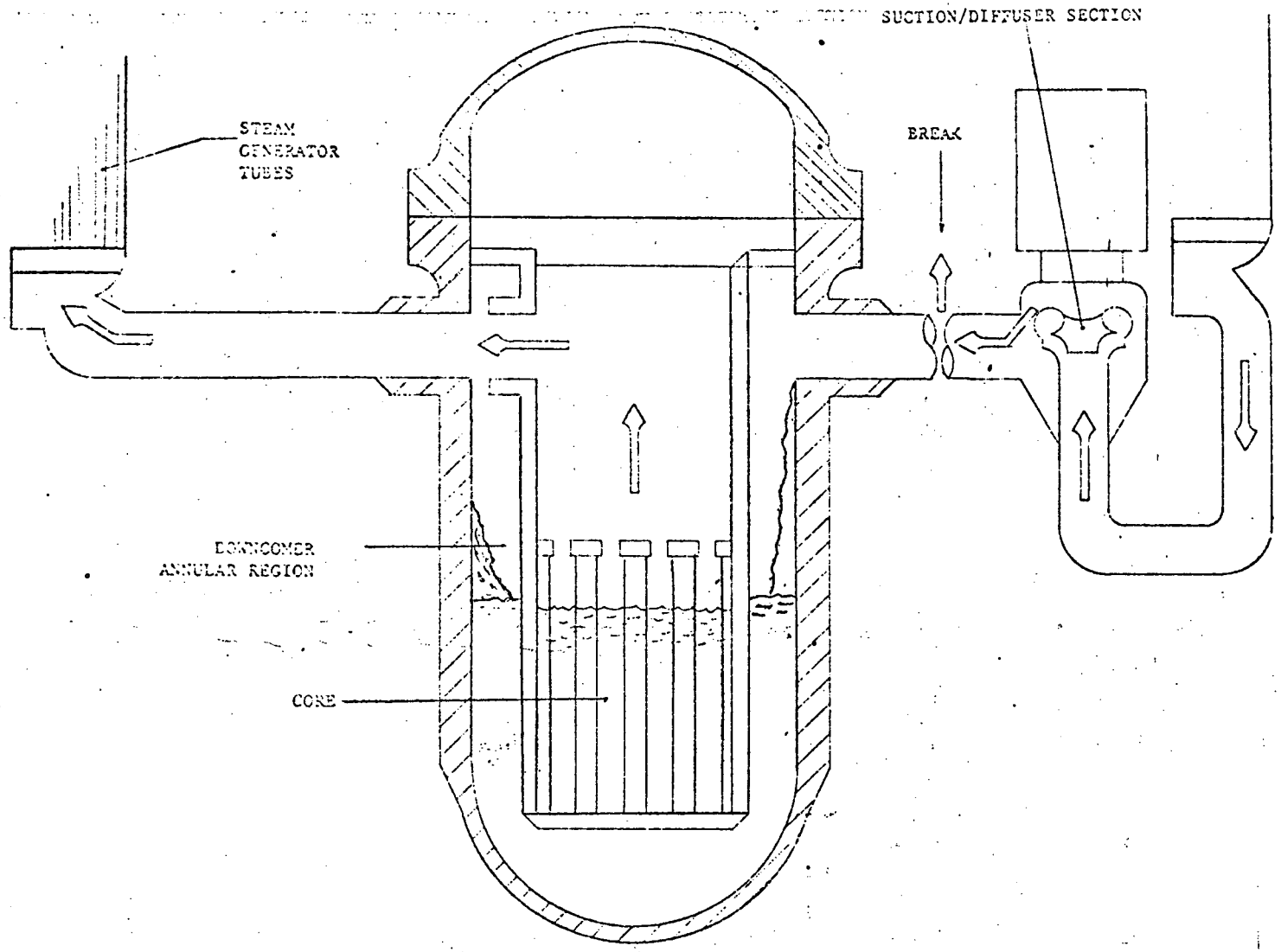

SUCTION/DIFFUSER SECTION
STEAM
GENERATOR
TUBES
BREAK
DOWNCOMER
ANNULAR REGION
CORE

FIGURE 4

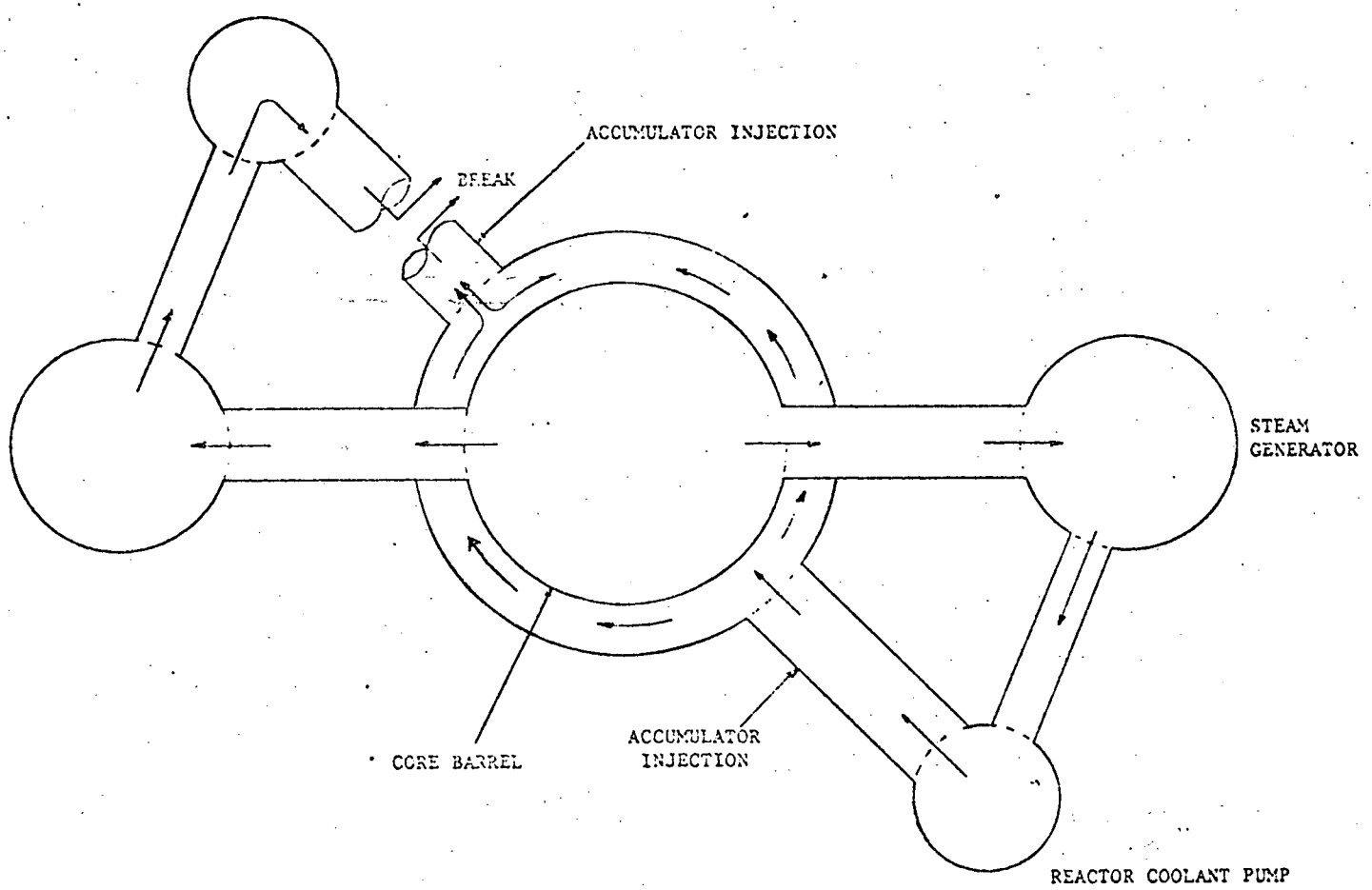

ACCUMULATOR INJECTION
BREAK
STEAM GENERATOR
CORE BARREL
ACCUMULATOR INJECTION
REACTOR COOLANT PUMP

FIGURE 5

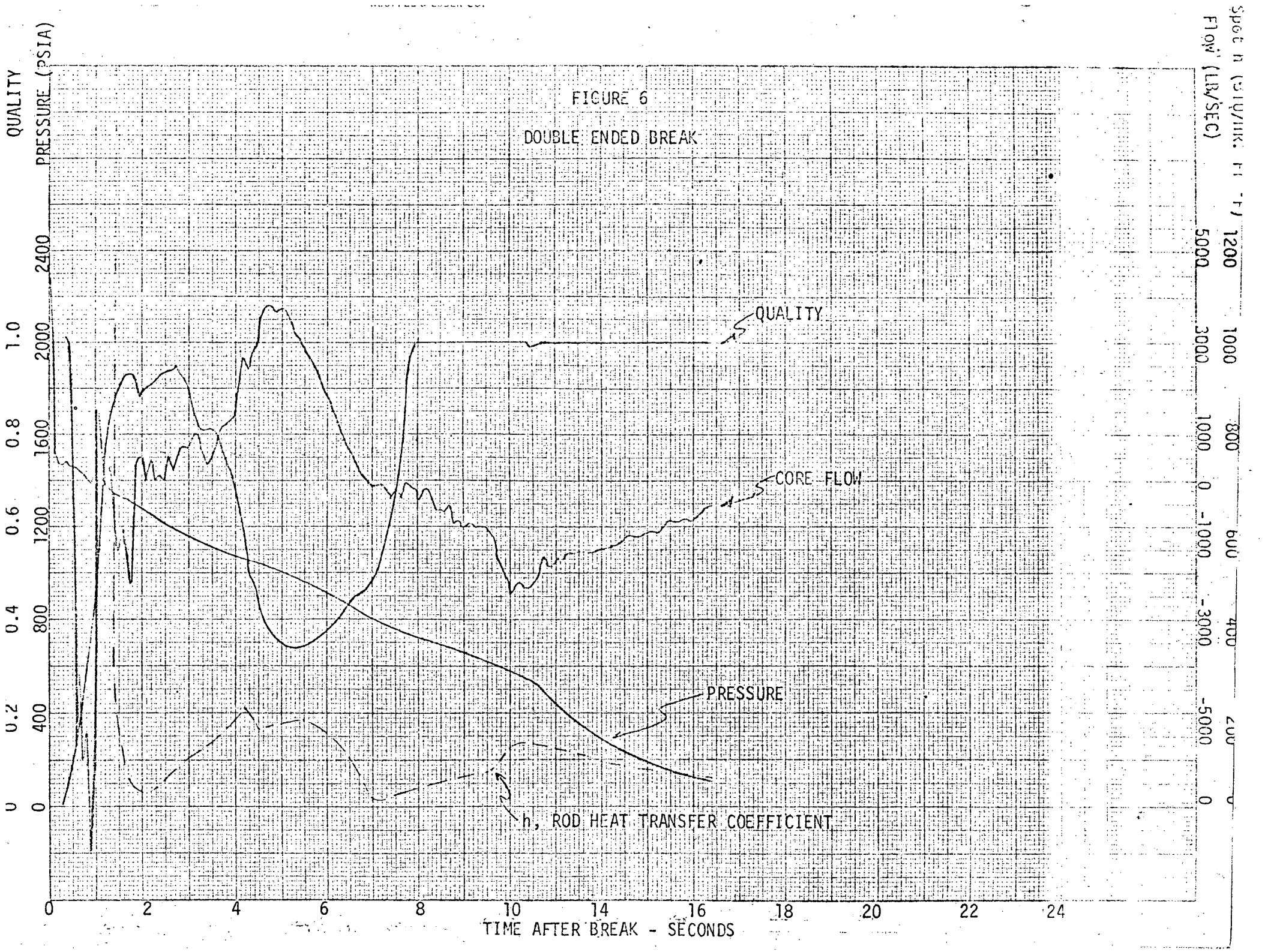


FIGURE 6
DOUBLE ENDED BREAK
QUALITY
PRESSURE (PSIA)
Flow (LB/SEC)
QUALITY
CORE FLOW
PRESSURE
h, ROD HEAT TRANSFER COEFFICIENT
TIME AFTER BREAK - SECONDS
0
0.2
0.4
0.6
0.8
1.0
400
800
1200
1600
2000
2400
-5000
-3000
-1000
0
1000
3000
5000
200
400
600
800
1000
1200
0
2
4
6
8
10
14
16
18
20
22
24

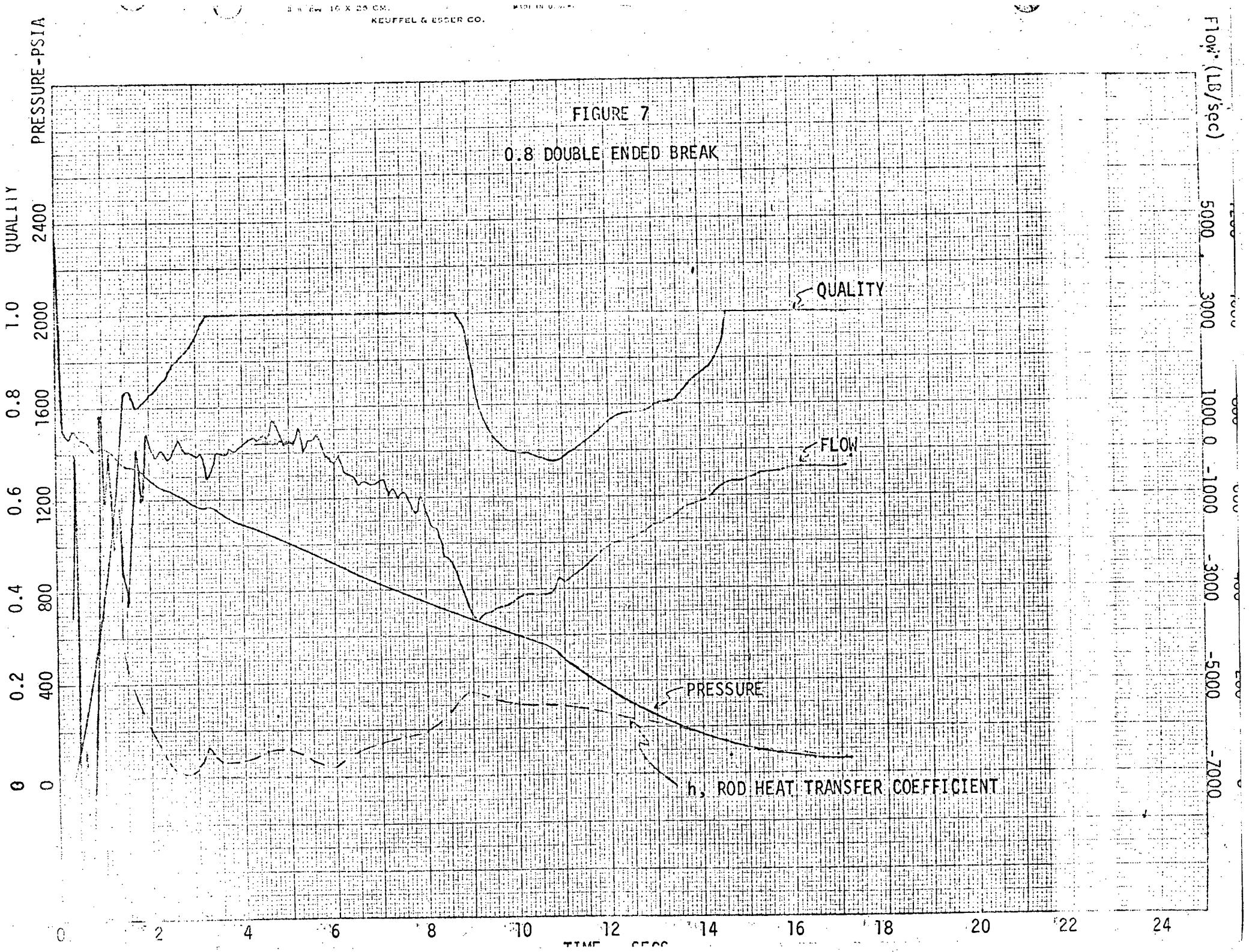


FIGURE 7
0.8 DOUBLE ENDED BREAK
QUALITY
FLOW
PRESSURE
h, ROD HEAT TRANSFER COEFFICIENT
PRESSURE-PSIA
QUALITY
Flow (LB/sec)
TIME SECS
0
0.2
0.4
0.6
0.8
1.0
400
800
1200
1600
2000
2400
5000
3000
1000
0
-1000
-3000
-5000
-7000
0
2
4
6
8
10
12
14
16
18
20
22
24

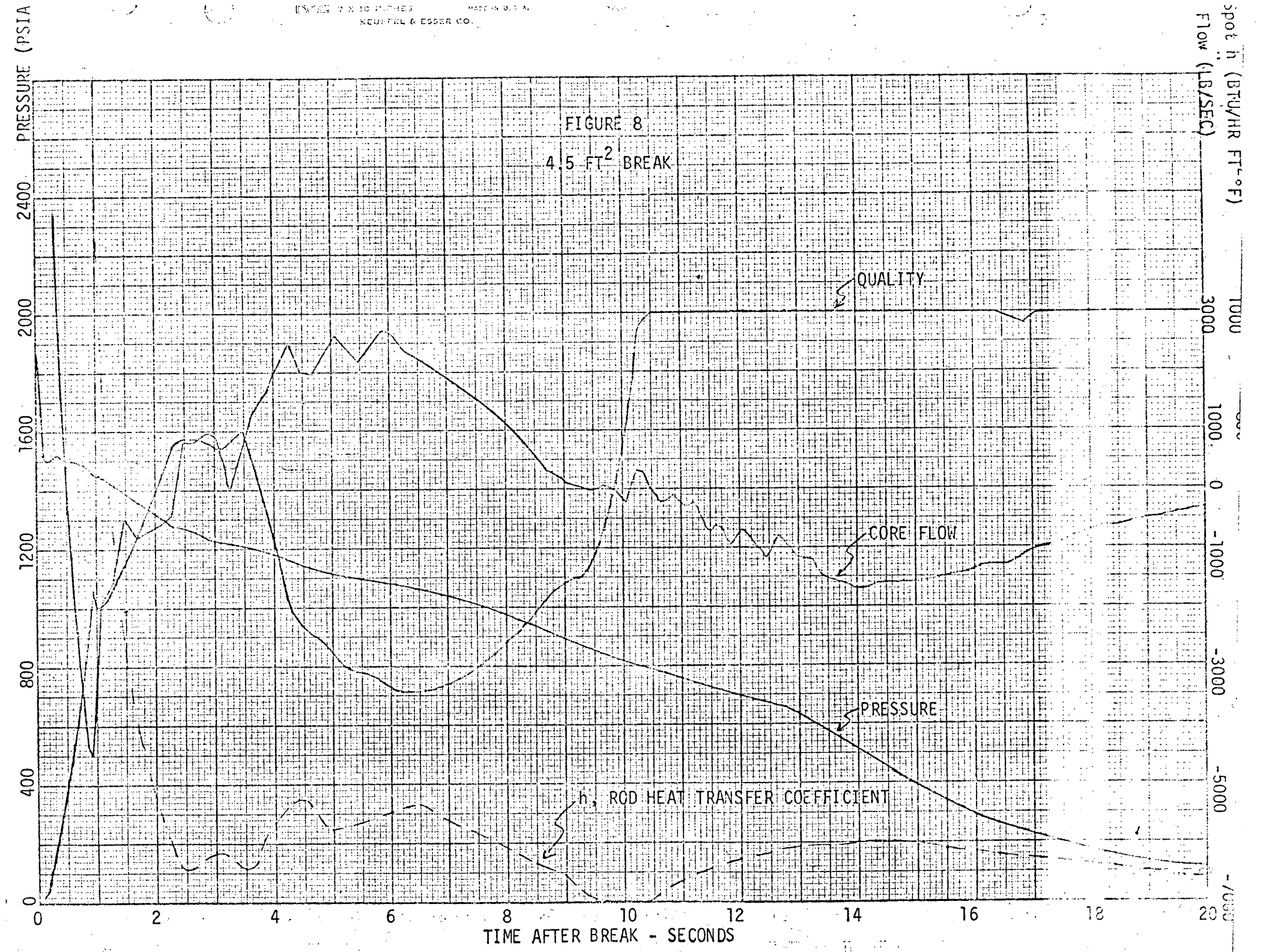


FIGURE 8
4.5 FT^2 BREAK
PRESSURE (PSIA)
Spot h (BTU/HR FT^2°F)
Flow (LB/SEC)
QUALITY
CORE FLOW
PRESSURE
h, ROD HEAT TRANSFER COEFFICIENT
TIME AFTER BREAK - SECONDS
0
400
800
1200
1600
2000
2400
1000
3000
1000
0
-1000
-3000
-5000
-7000
0
2
4
6
8
10
12
14
16
18
20

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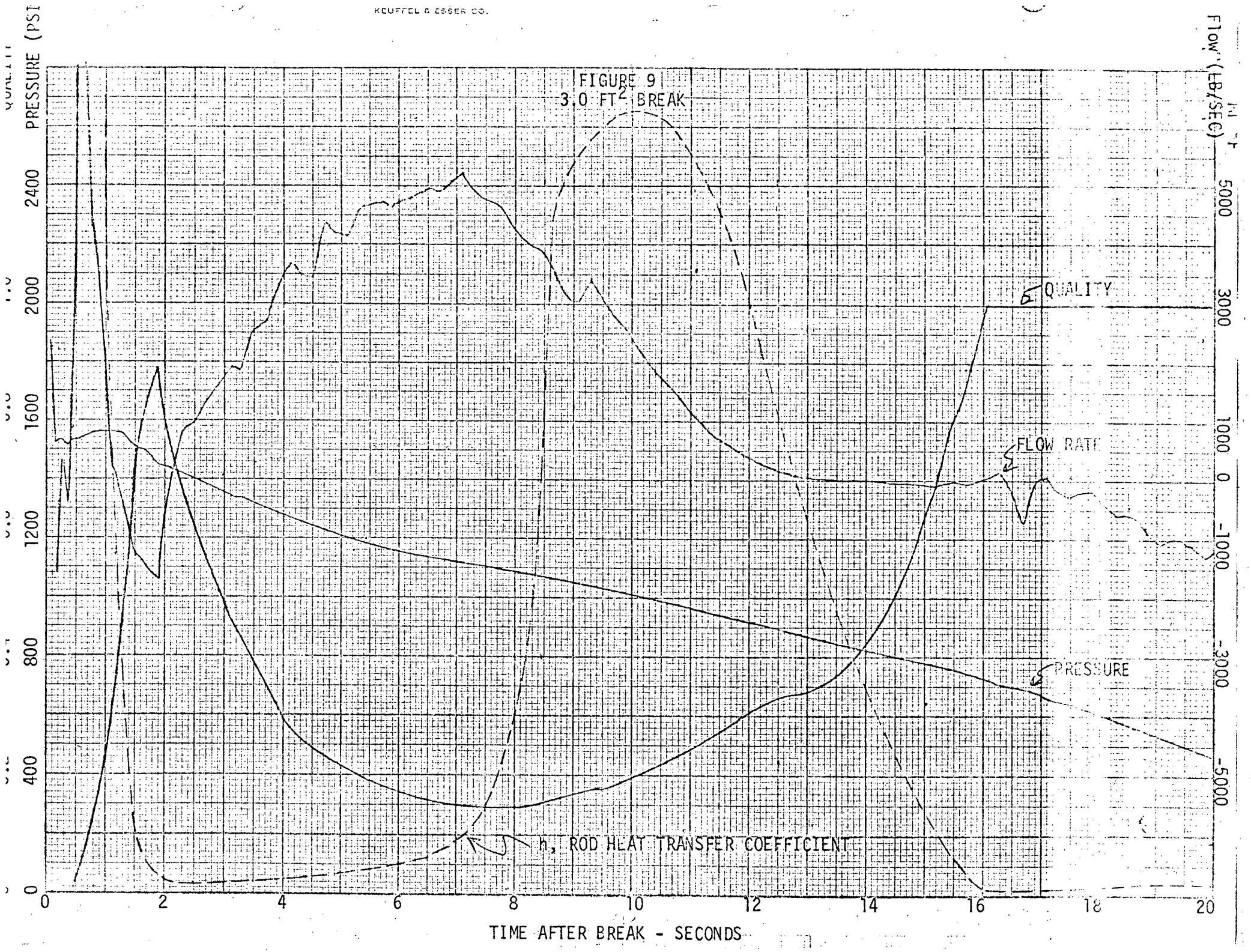

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FIGURE 10

PEAK CLAD TEMPERATURE vs TIME AFTER BREAK

DOUBLE ENDED BREAK

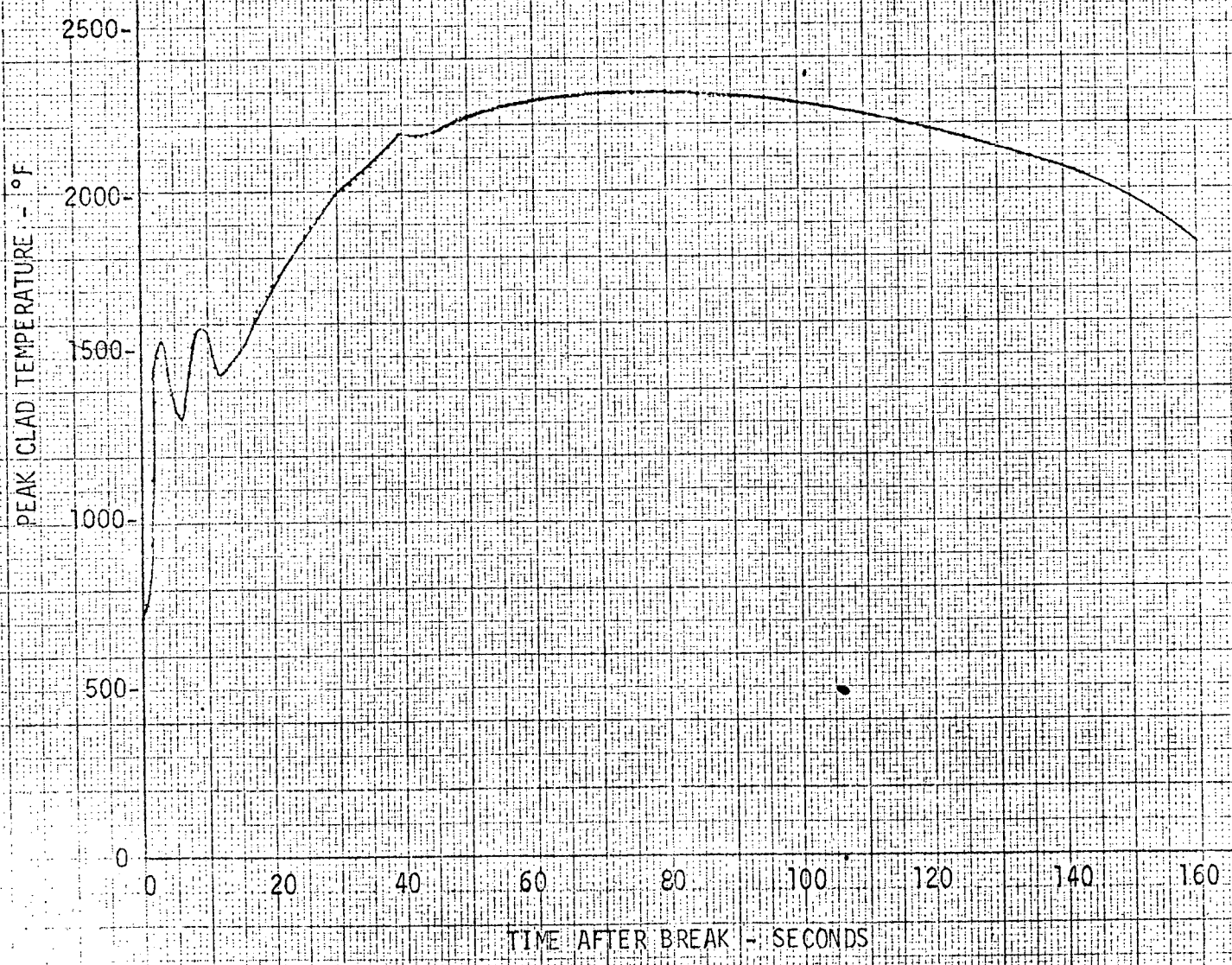

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FIGURE 11

PEAK CLAD TEMPERATURE vs TIME AFTER BREAK

.8 DOUBLE ENDED BREAK

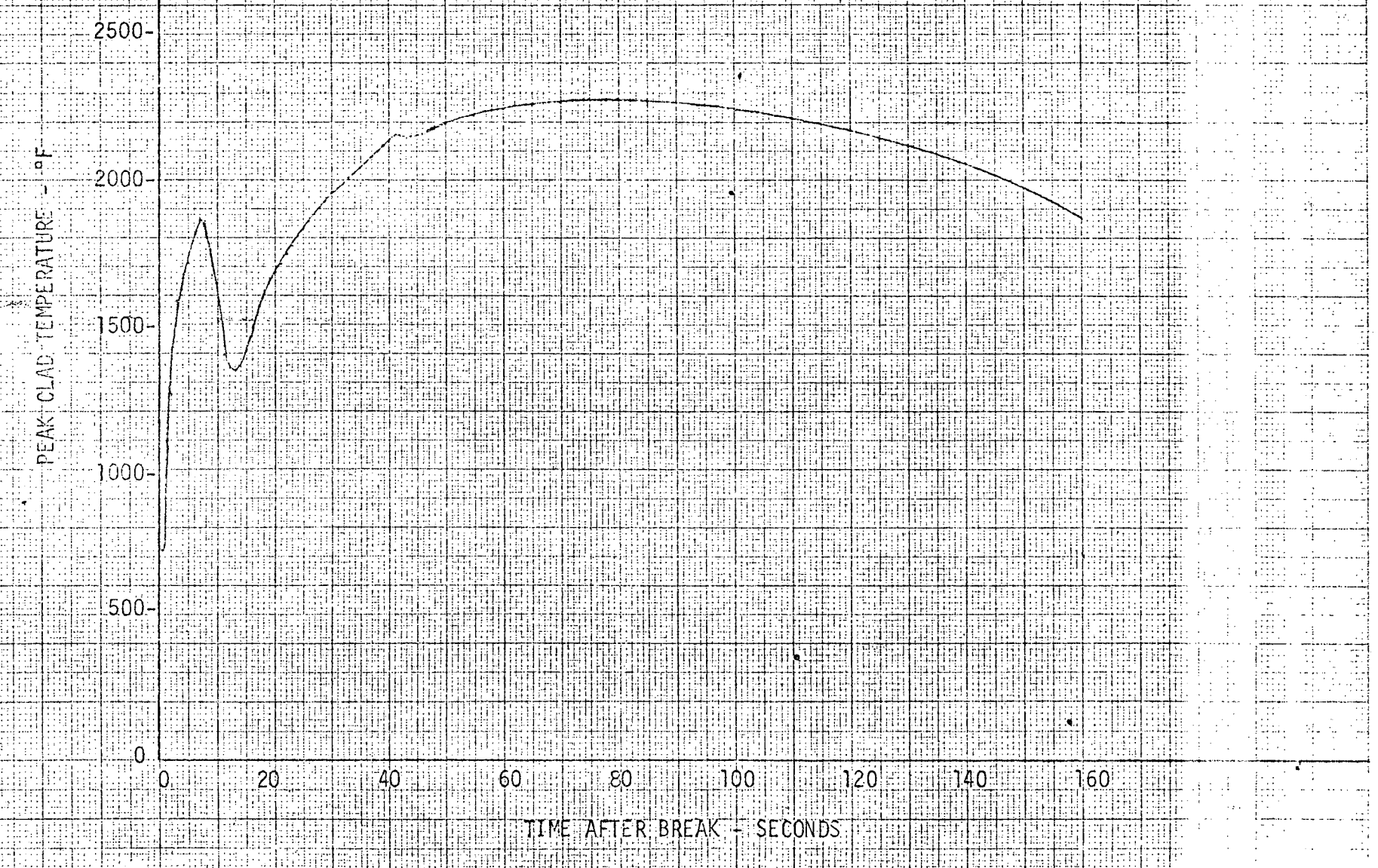

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FIGURE-12

PEAK CLAD TEMPERATURE vs TIME AFTER BREAK

4.5 FT^2 BREAK

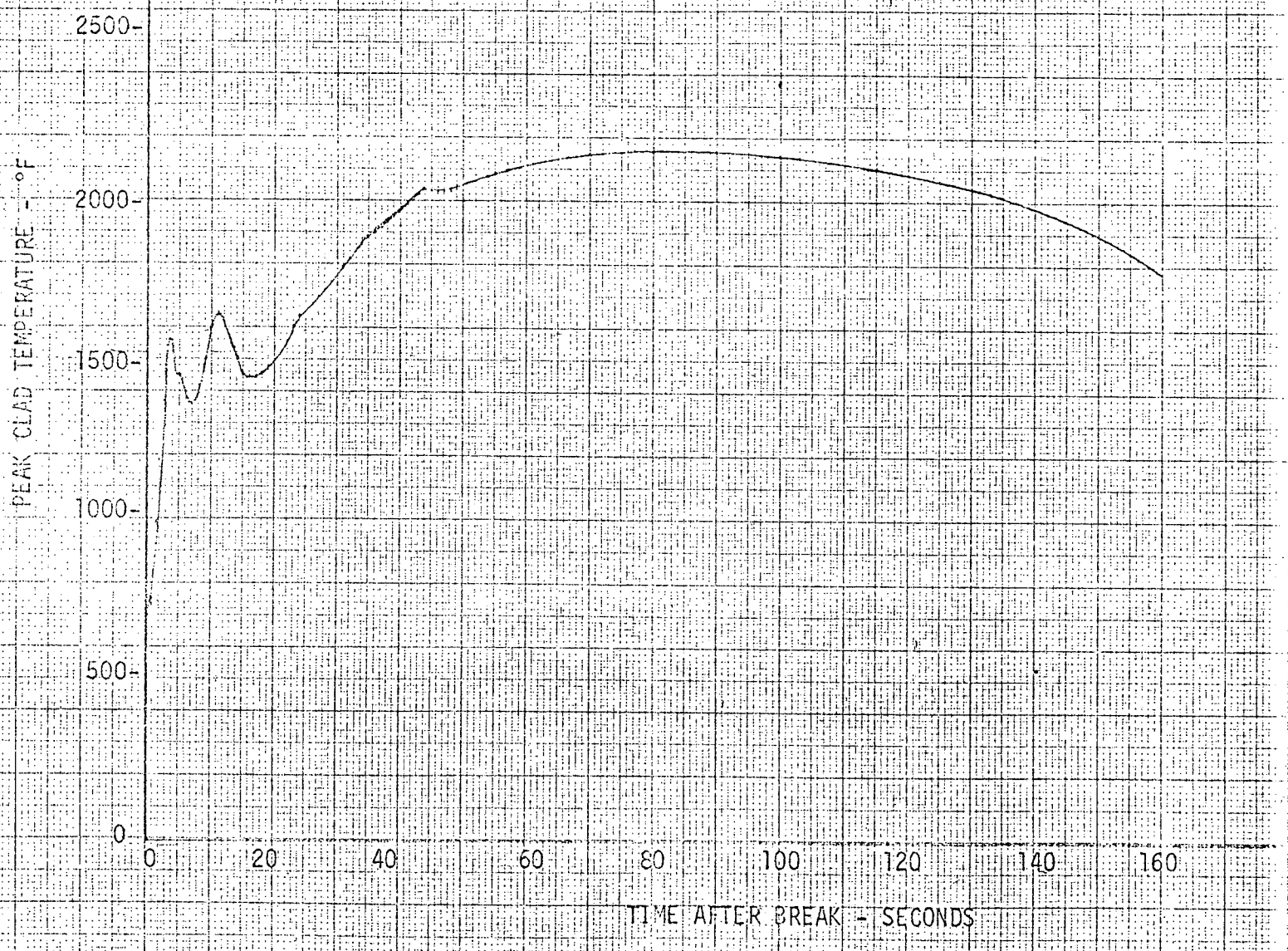

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FIGURE 13

PEAK CLAD TEMPERATURE vs TIME AFTER BREAK

3 FT^2 BREAK

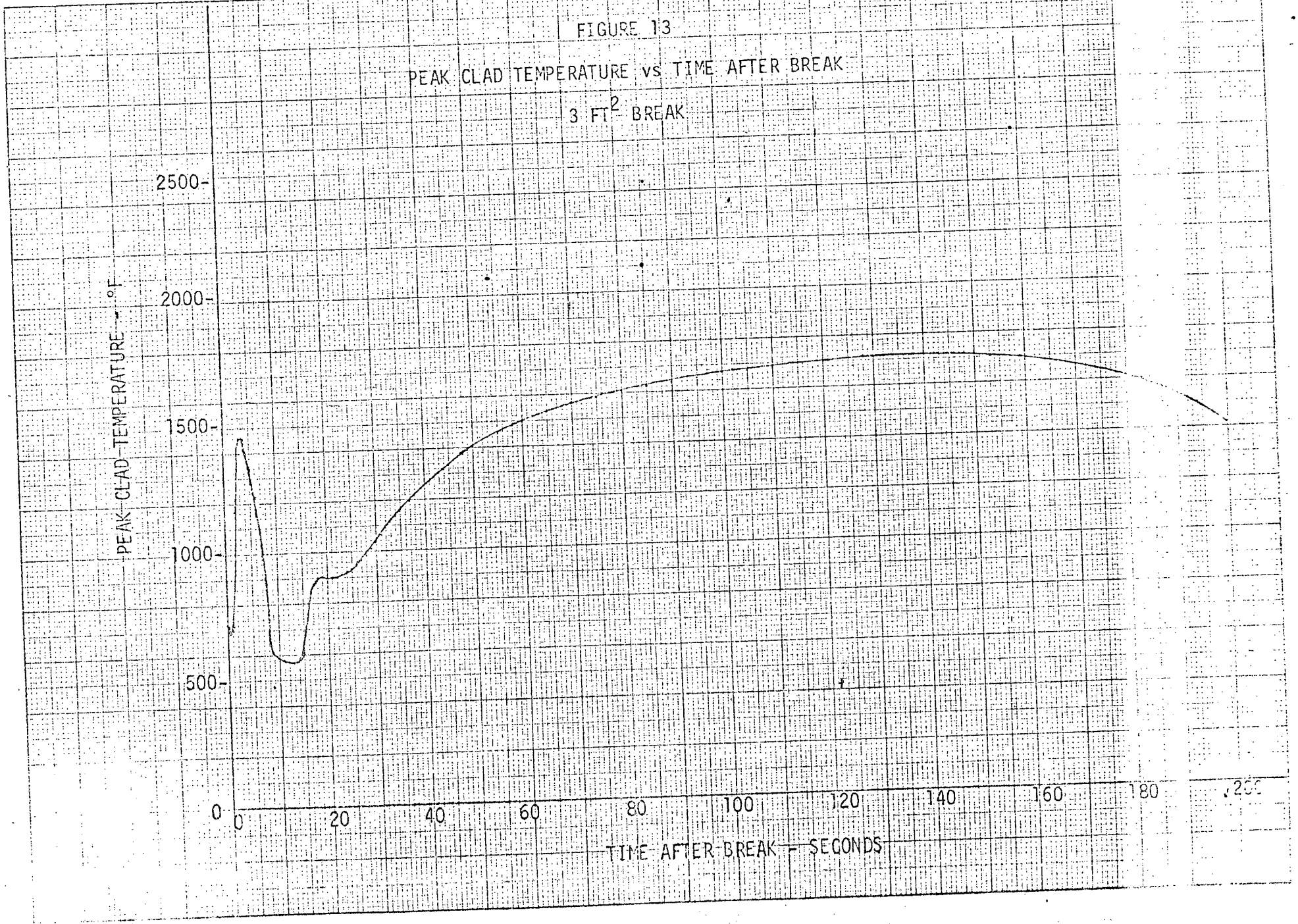

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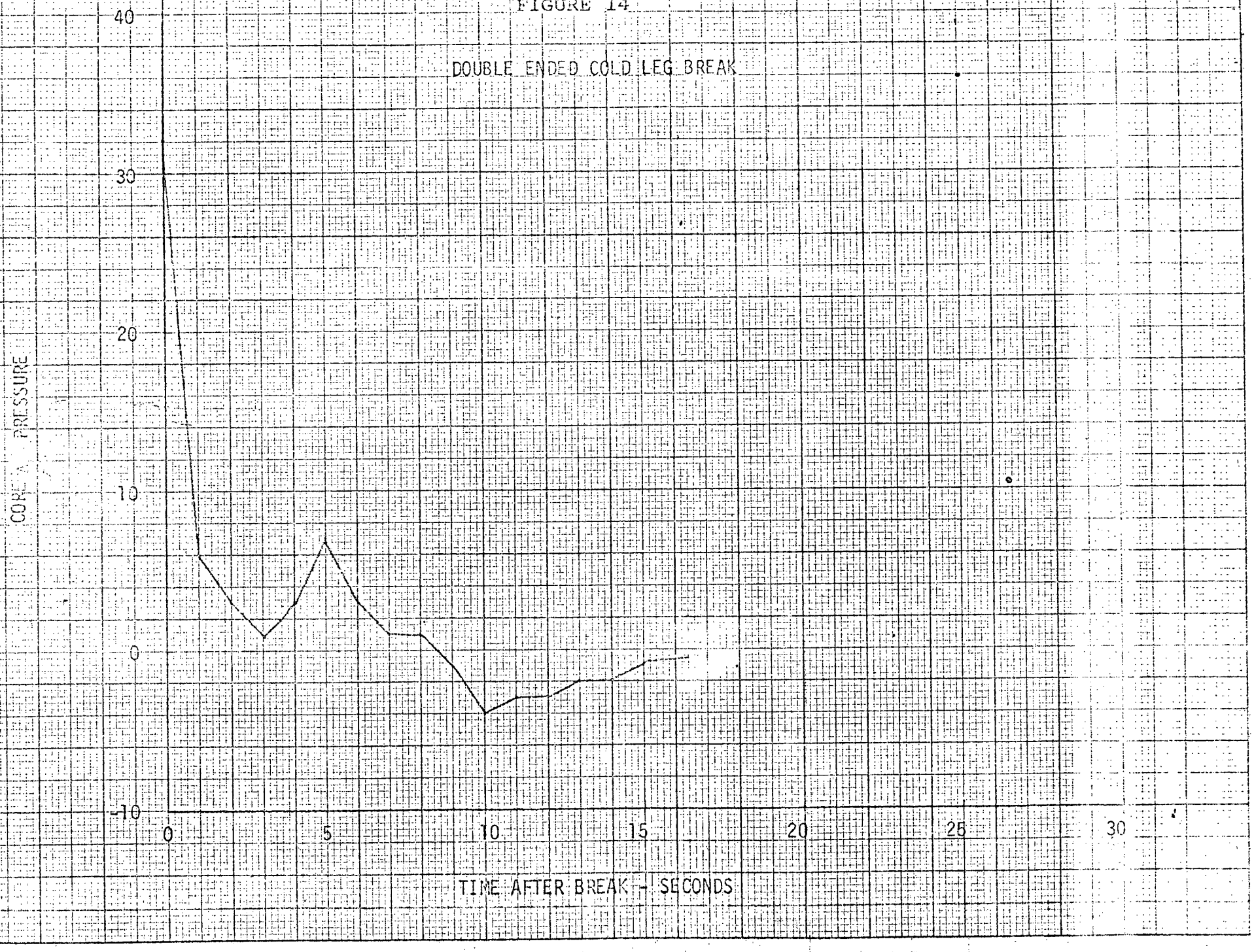
FIGURE 14

DOUBLE ENDED COLD LEG BREAK



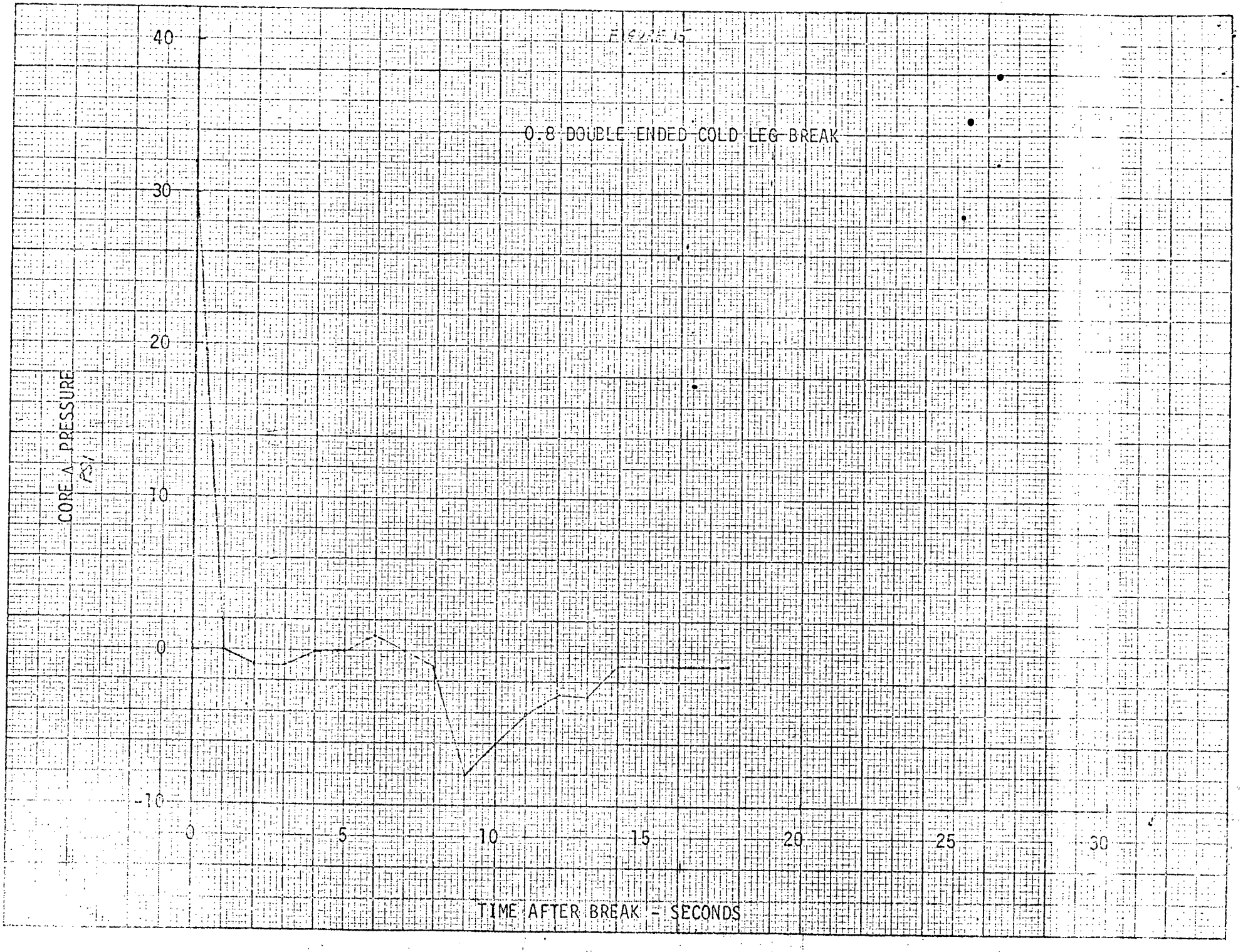

0.8 DOUBLE ENDED COLD LEG BREAK
CORE Δ PRESSURE PSI
40
30
20
10
0
-10
0
5
10
15
20
25
30
TIME AFTER BREAK - SECONDS

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Figure 16

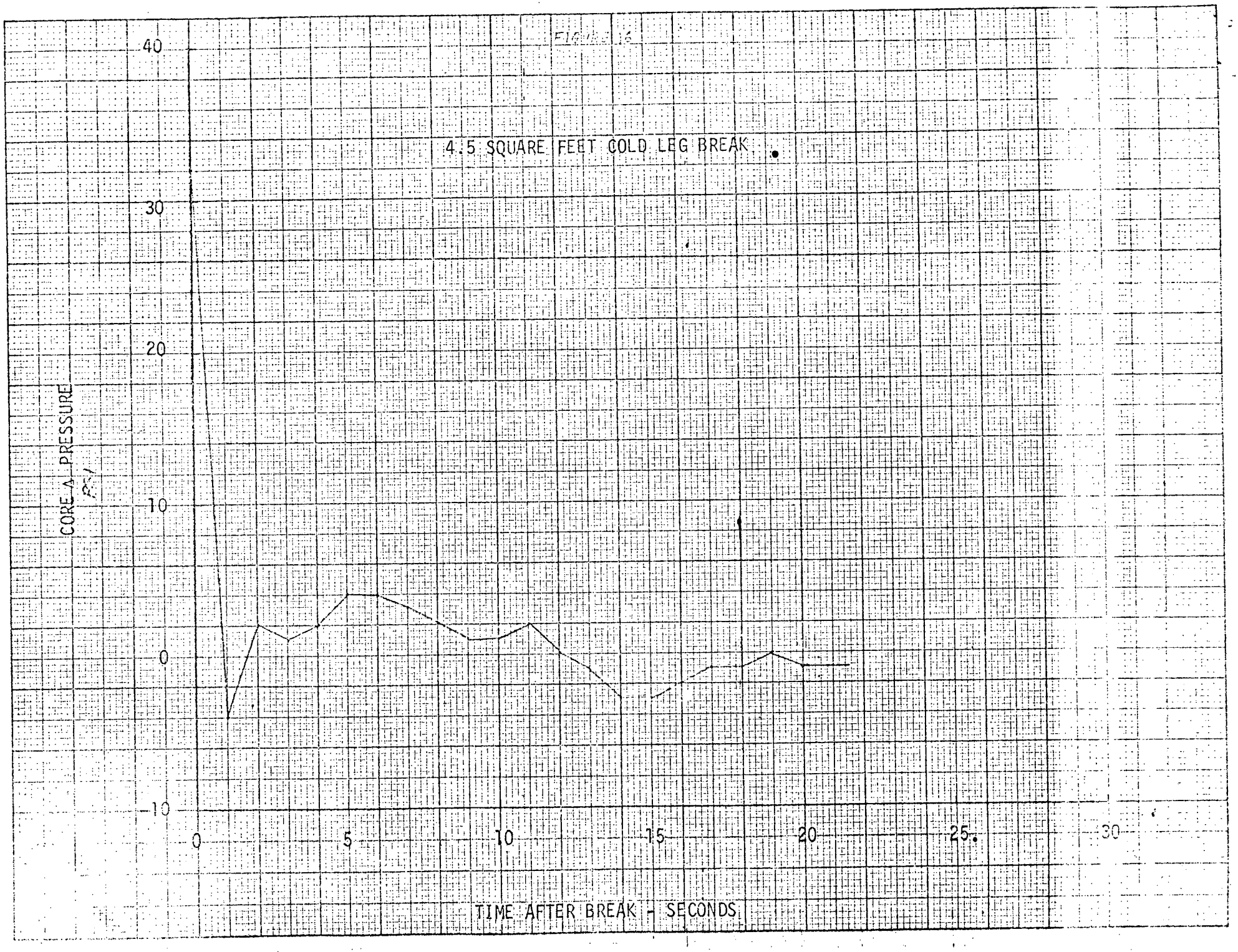

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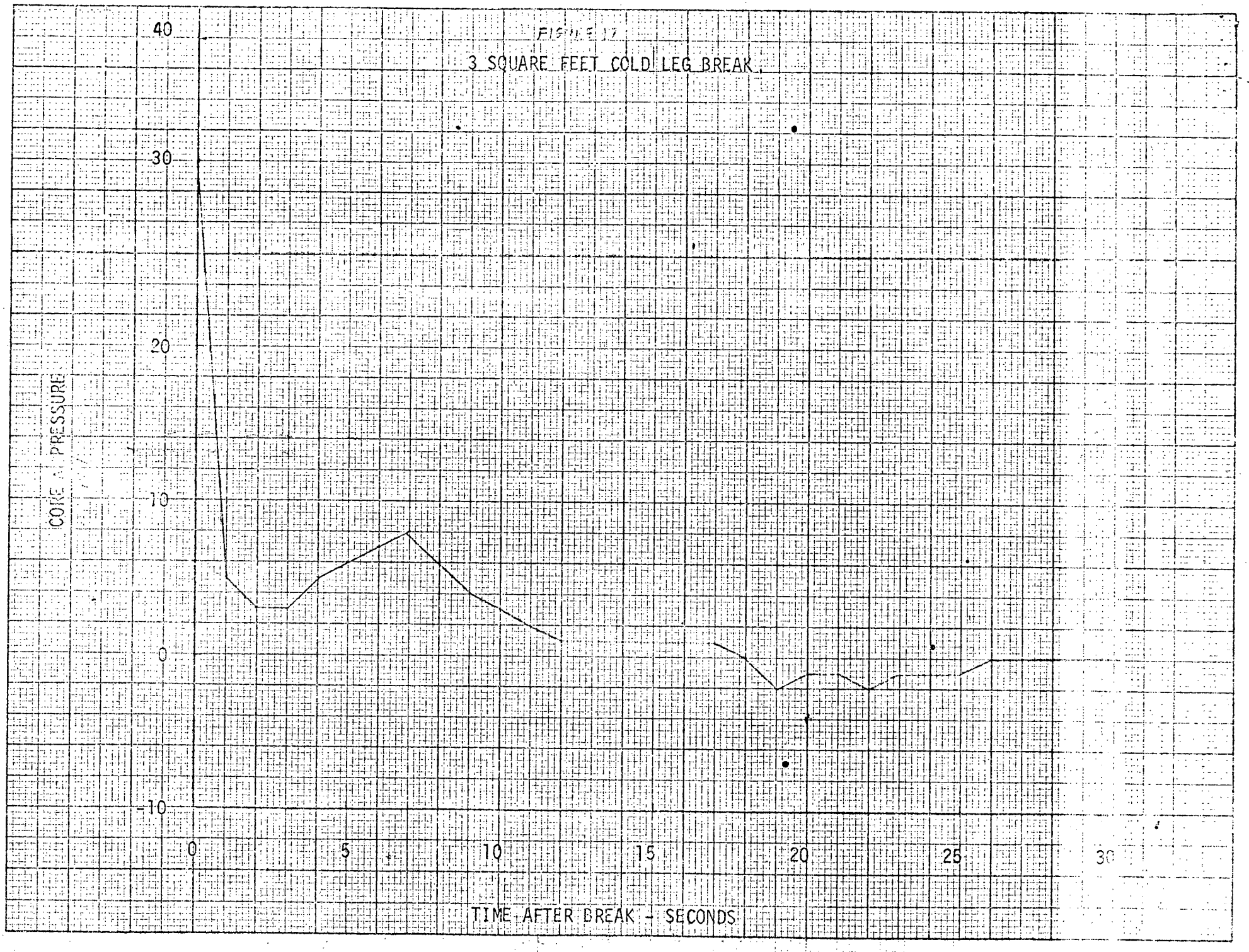
FIGURE 17

3 SQUARE FEET COLD LEG BREAK



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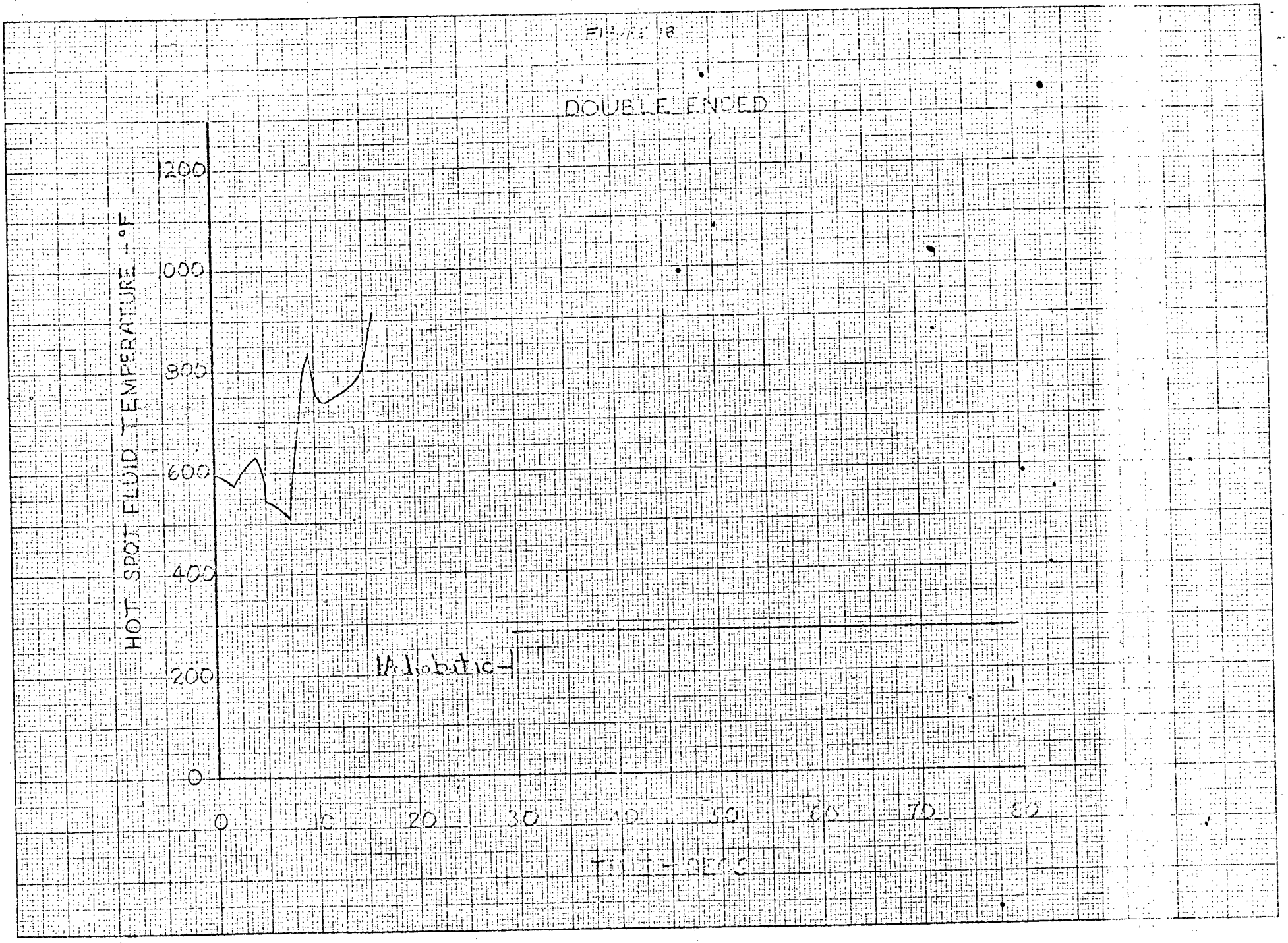

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FIGURE 19

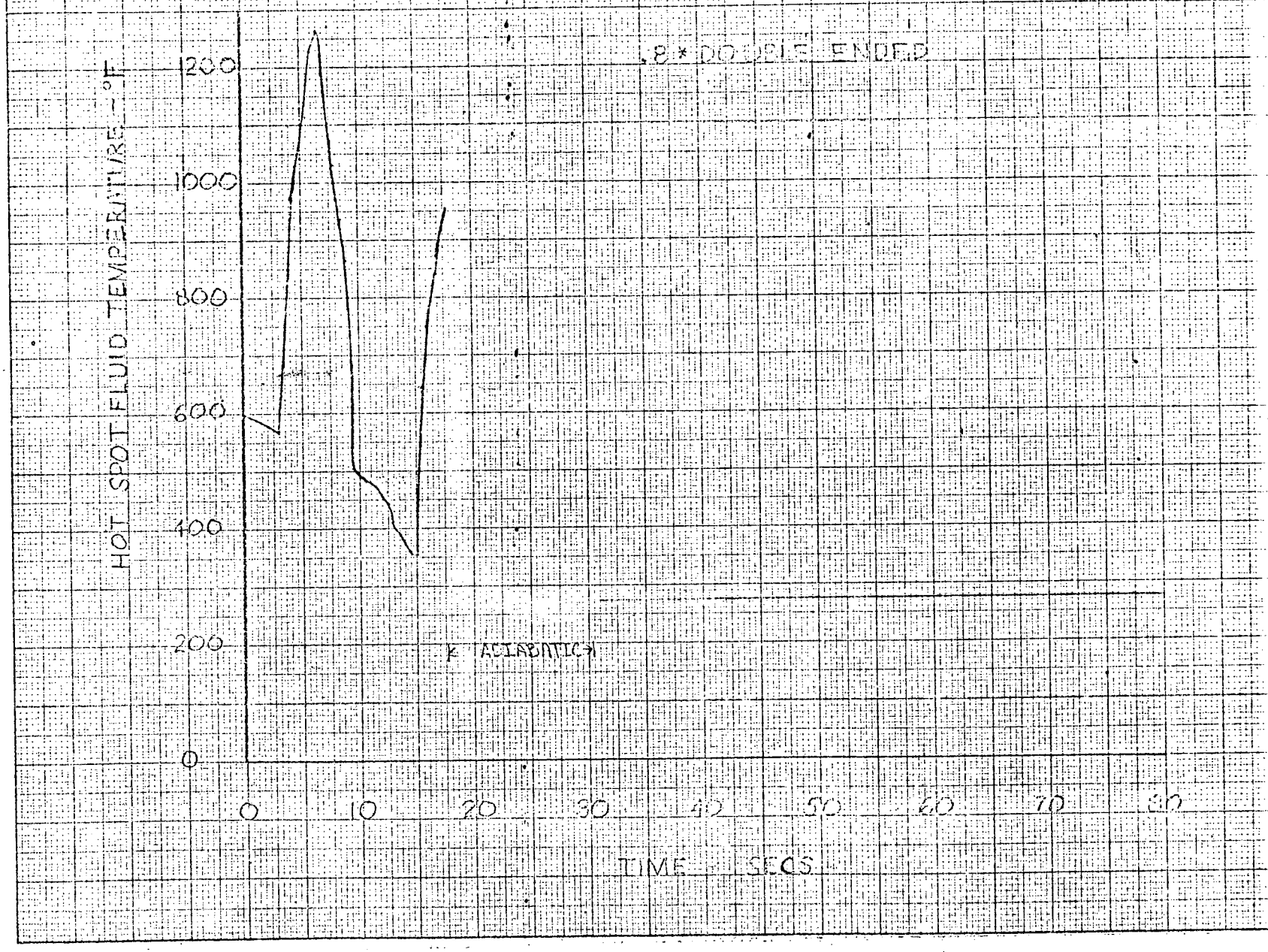

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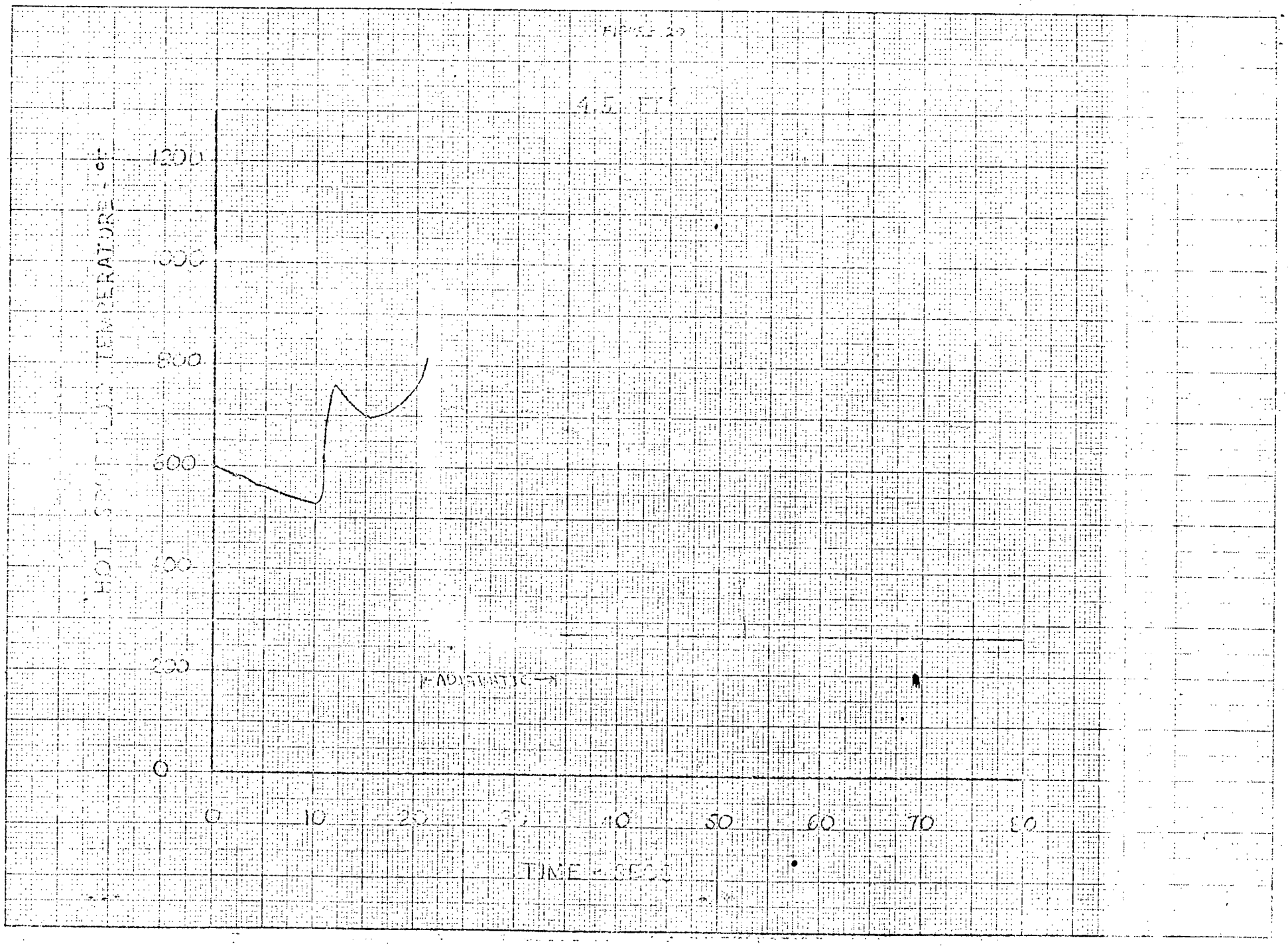
FIGURE 20



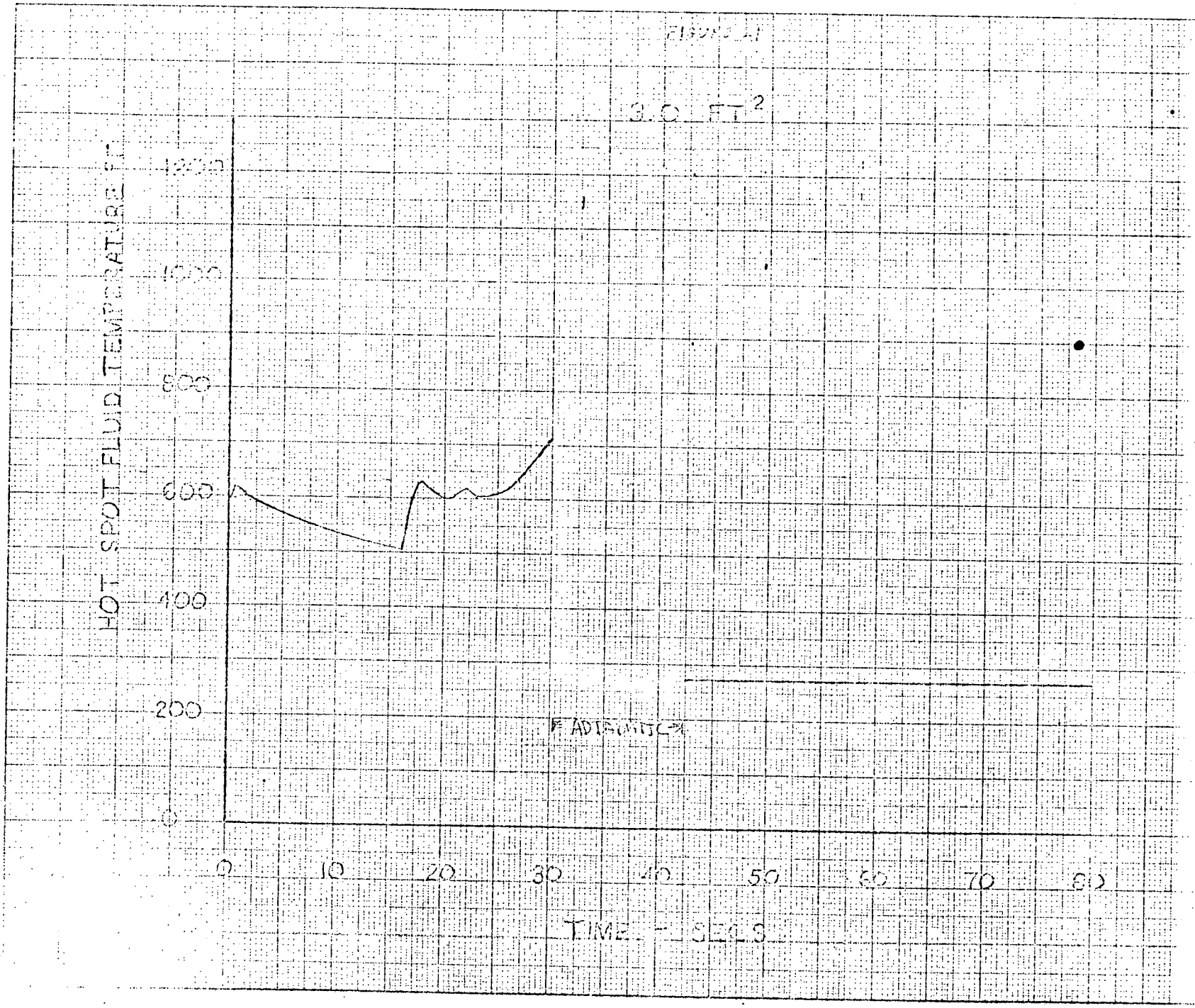

3.0 FT 2
HOT SPOT FLUID TEMPERATURE F
1200
1000
800
600
400
200
0
ADIABATIC
0
10
20
30
40
50
60
70
80
TIME - SECS

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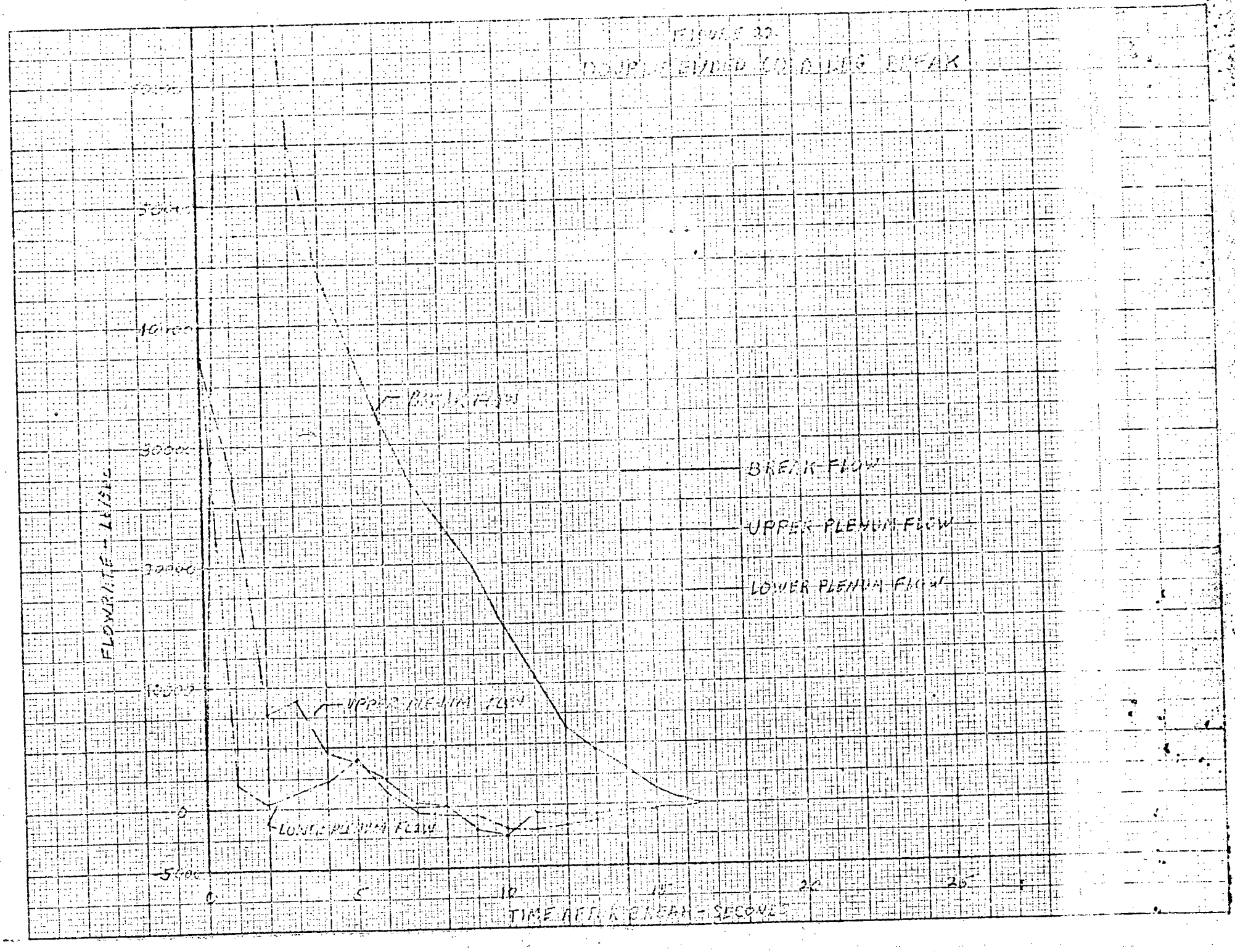
FIGURE 22

DOUBLE ENDED COOLING BREAK



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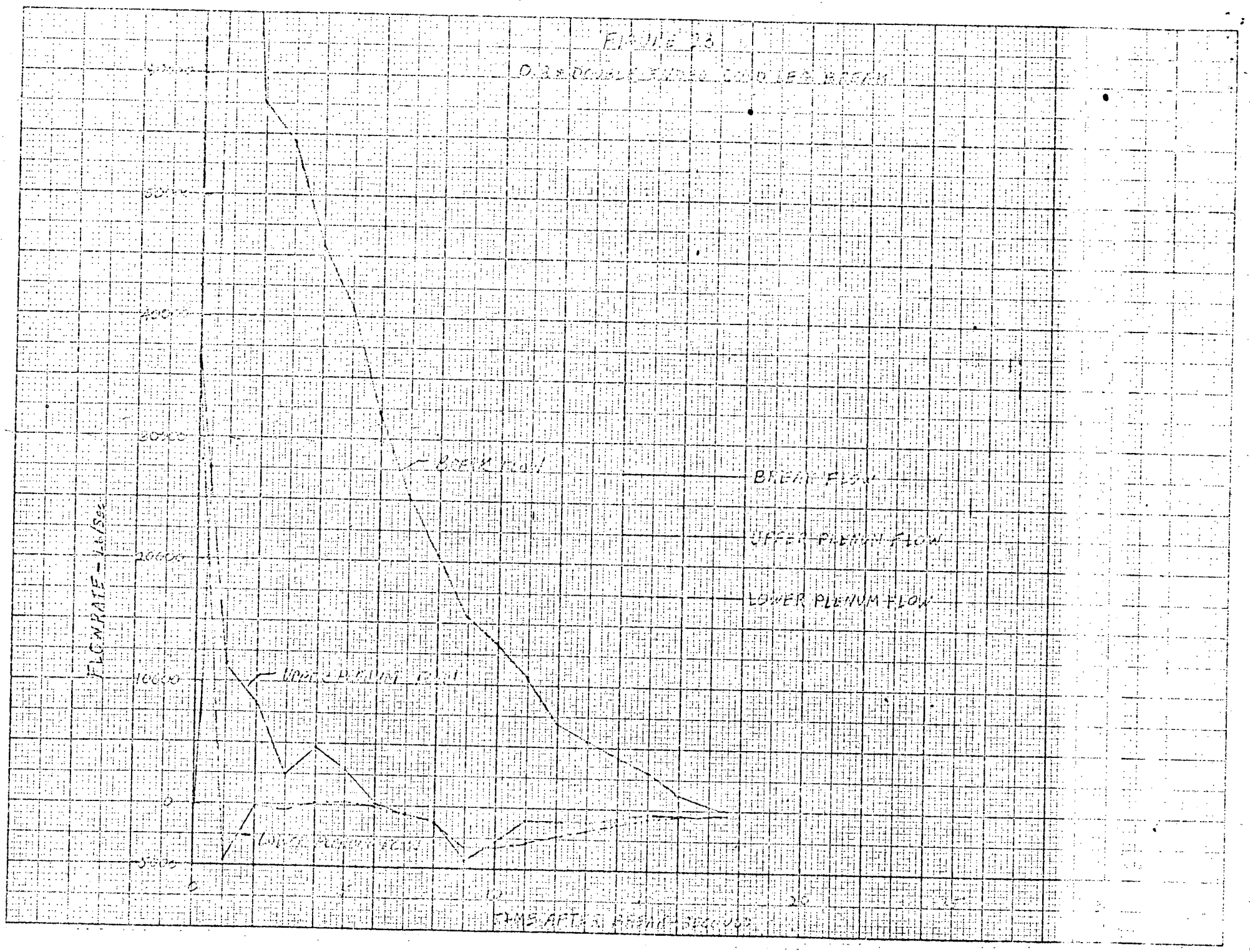

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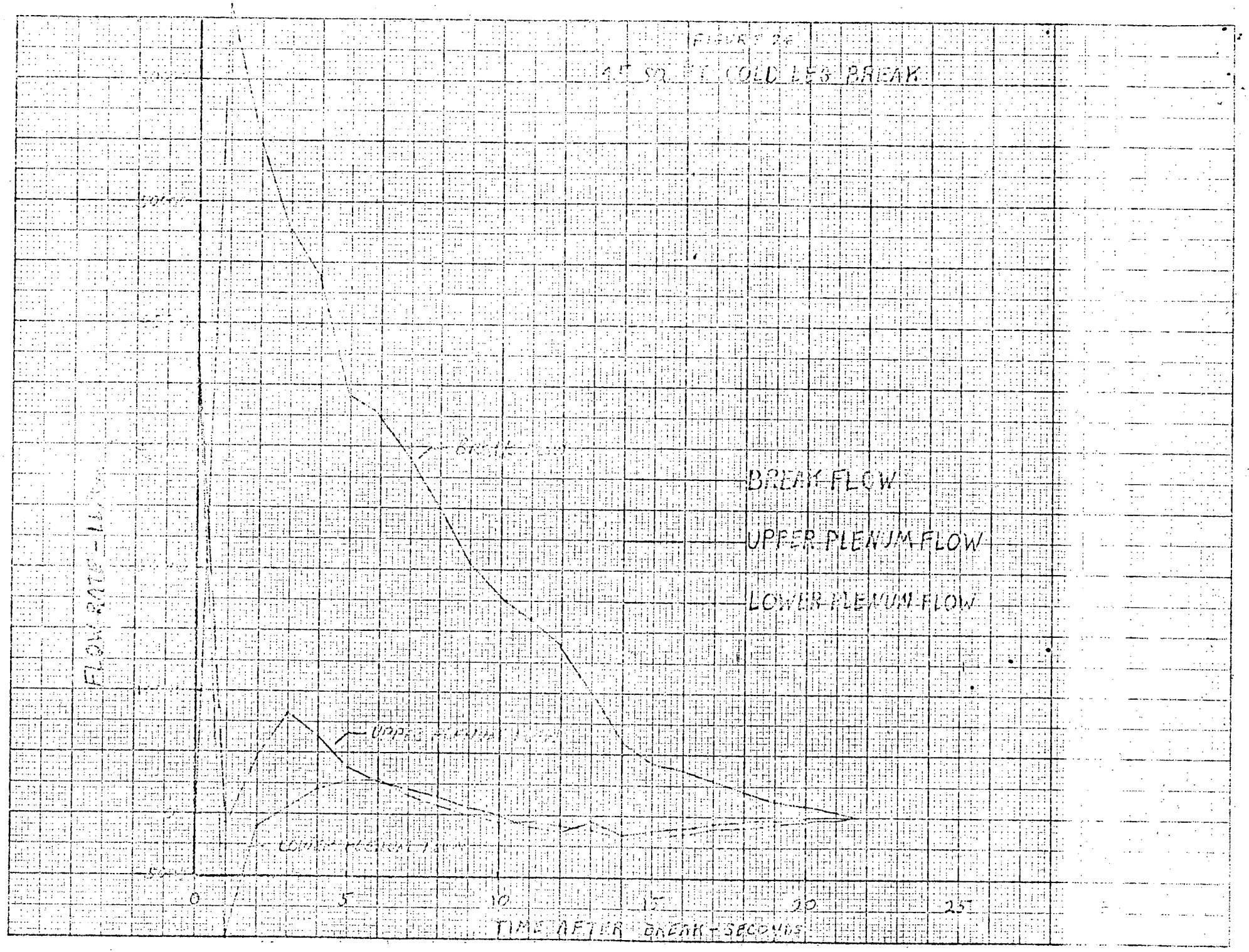
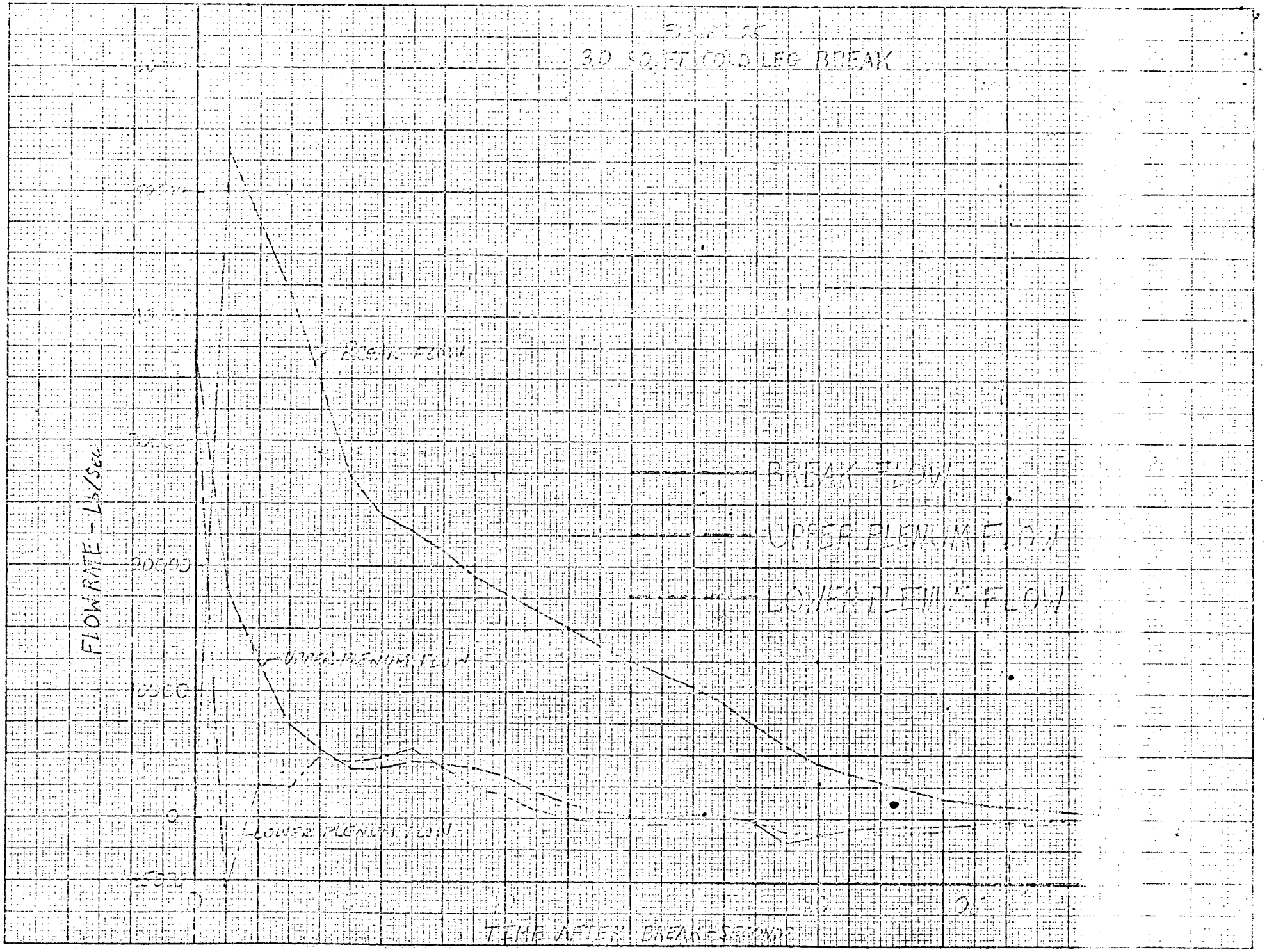
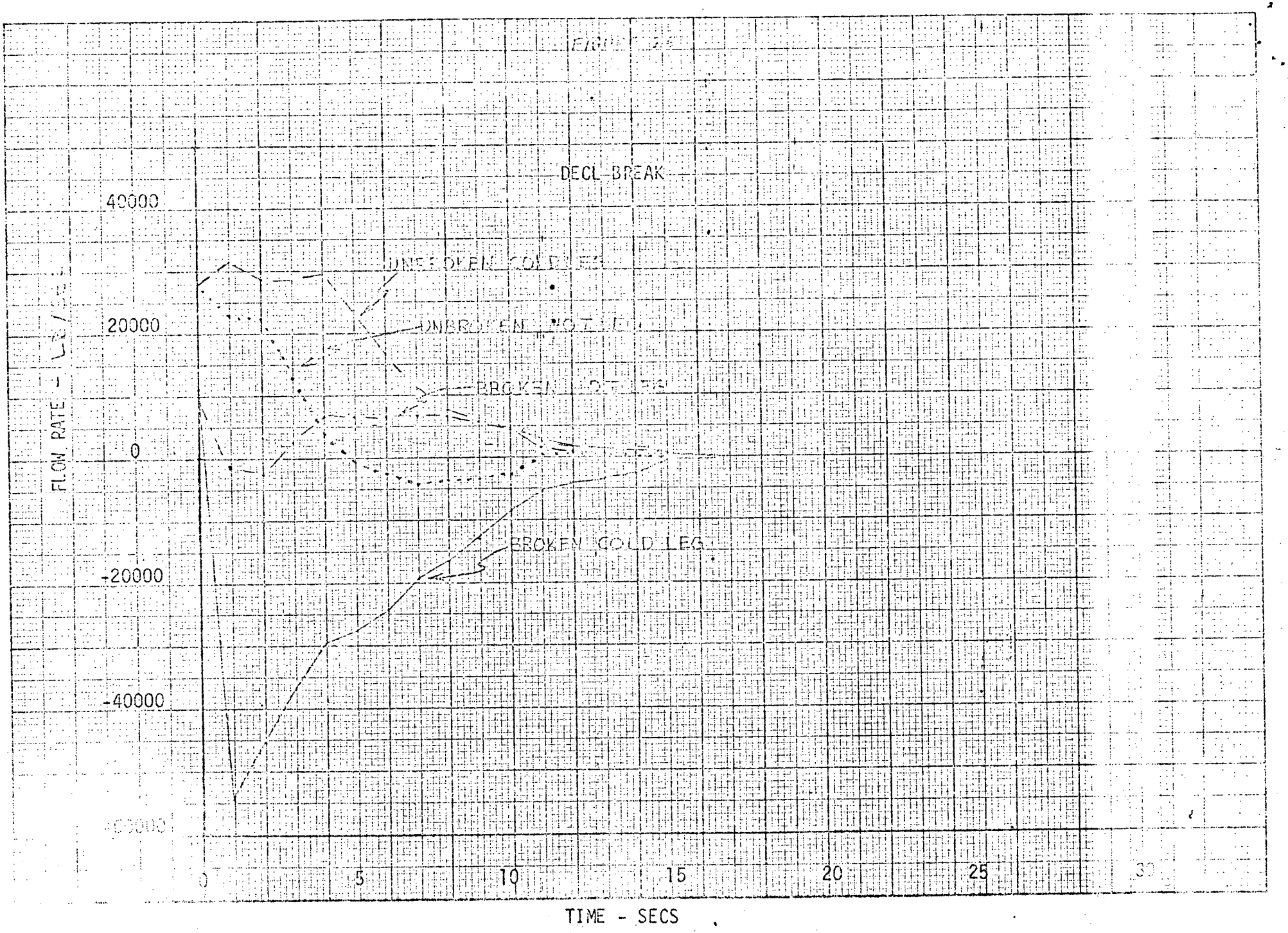
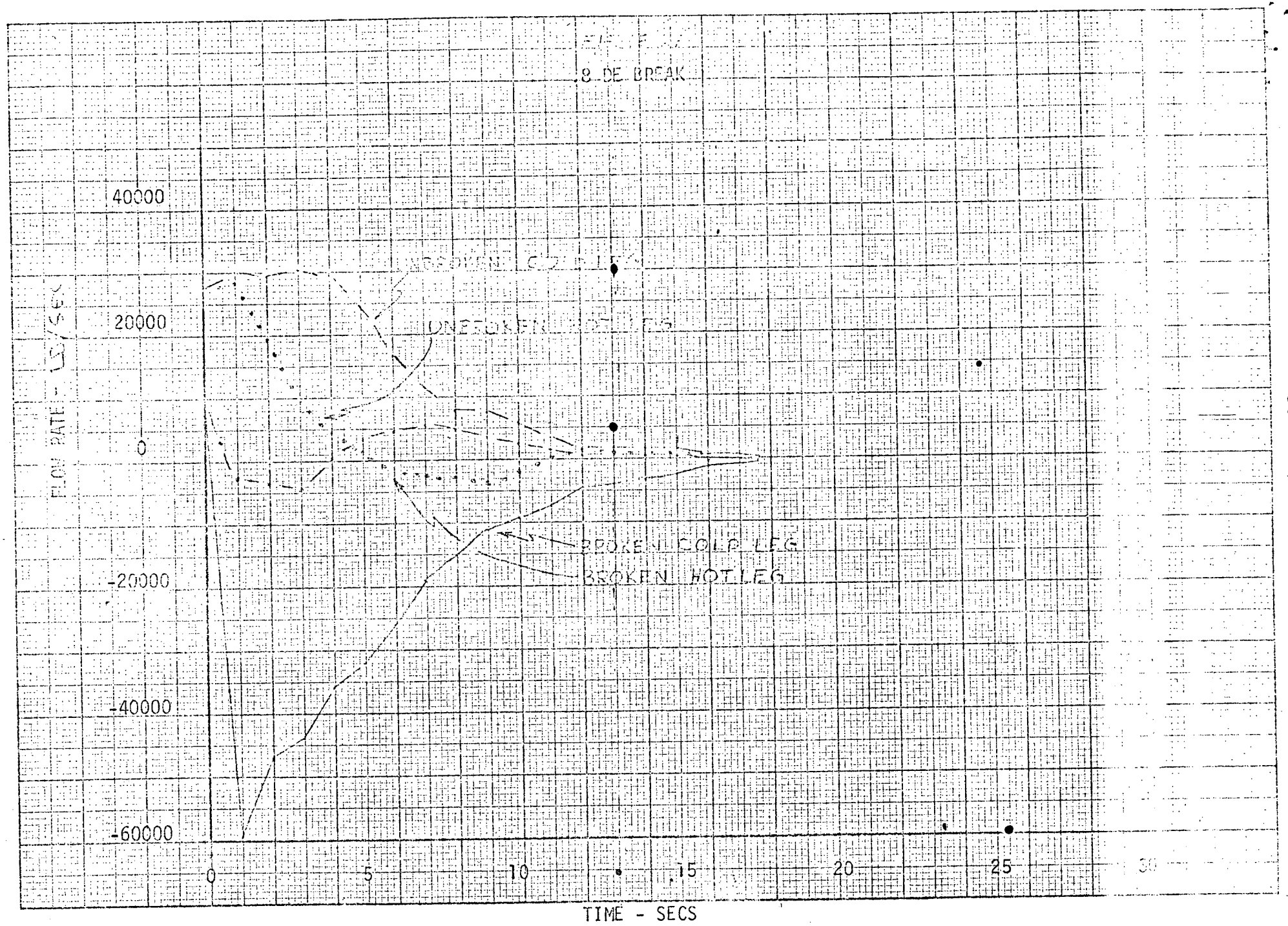
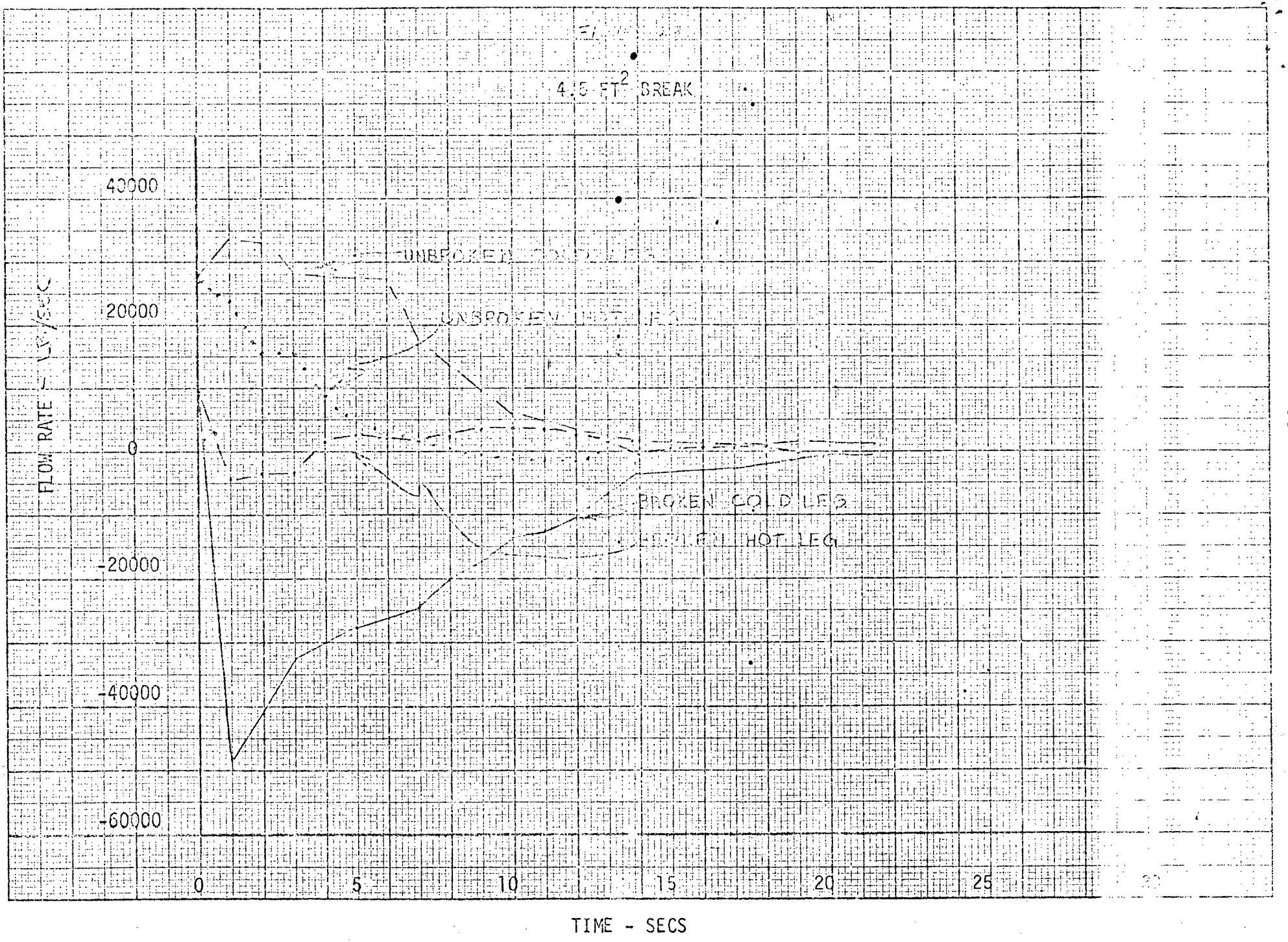


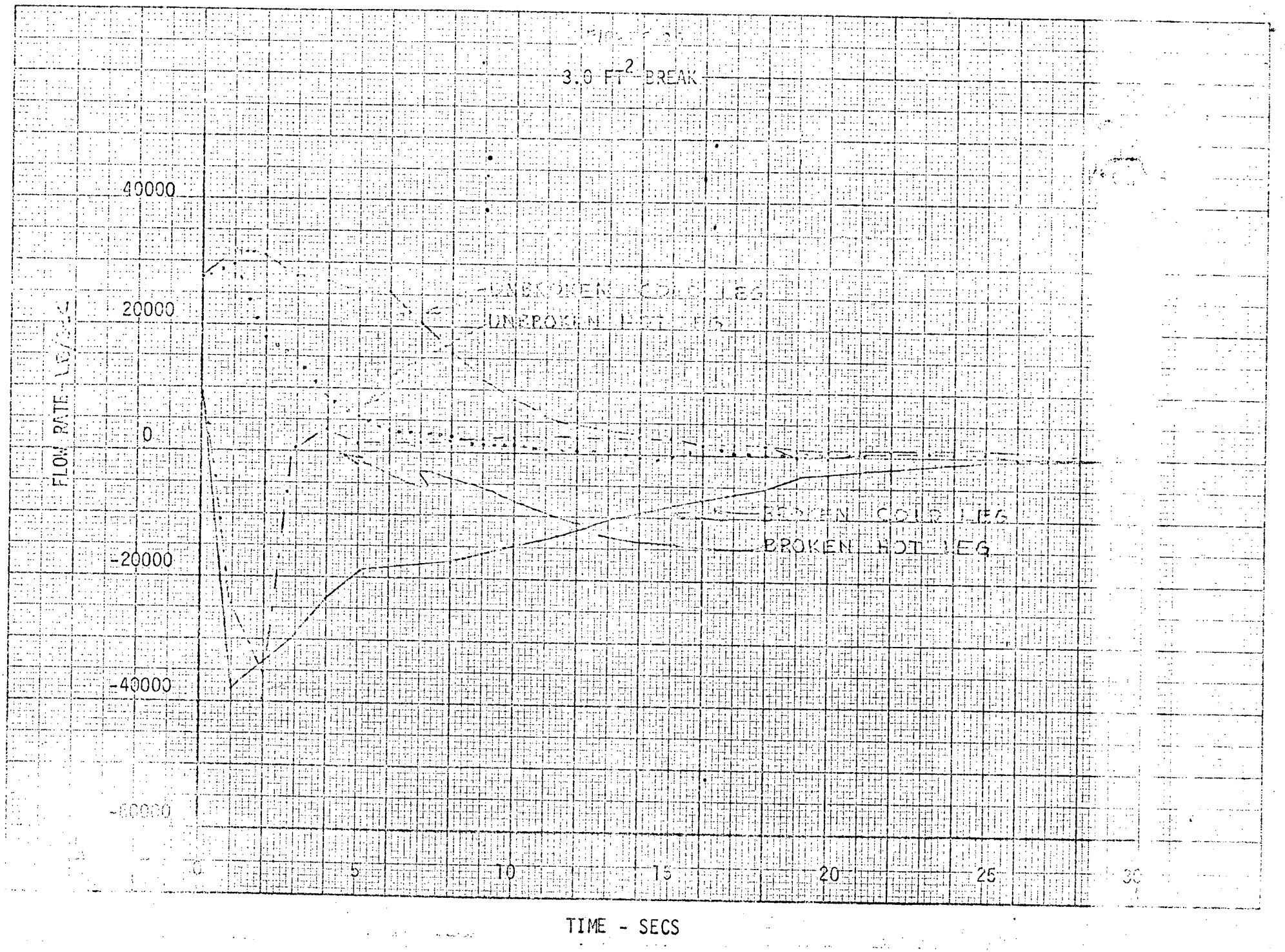
KEUFFEL & ESSER CO.




DECL-BREAK
UNBROKEN COLD LEG
UNBROKEN HOT LEG
BROKEN HOT LEG
BROKEN COLD LEG
40000
20000
0
-20000
-40000
FLOW RATE - LB/SEC
0
5
10
15
20
25
30
TIME - SECS


.8 DE BREAK
BROKEN COLD LEG
UNBROKEN LOOPS
BROKEN COLD LEG
BROKEN HOT LEG
40000
20000
0
-20000
-40000
-60000
FLOW RATE - LB/SEC
0
5
10
15
20
25
30
TIME - SECS


4.5 FT^2 BREAK
UNBROKEN COLD LEG
UNBROKEN HOT LEG
BROKEN COLD LEG
BROKEN HOT LEG
FLOW RATE - LB/SEC
40000
20000
0
-20000
-40000
-60000
0
5
10
15
20
25
TIME - SECS


3.0 FT^2 BREAK
40000
20000
0
-20000
-40000
-60000
FLOW RATE
UNBROKEN COLD LEG
UNBROKEN HOT LEG
BROKEN COLD LEG
BROKEN HOT LEG
0
5
10
15
20
25
30
TIME - SECS

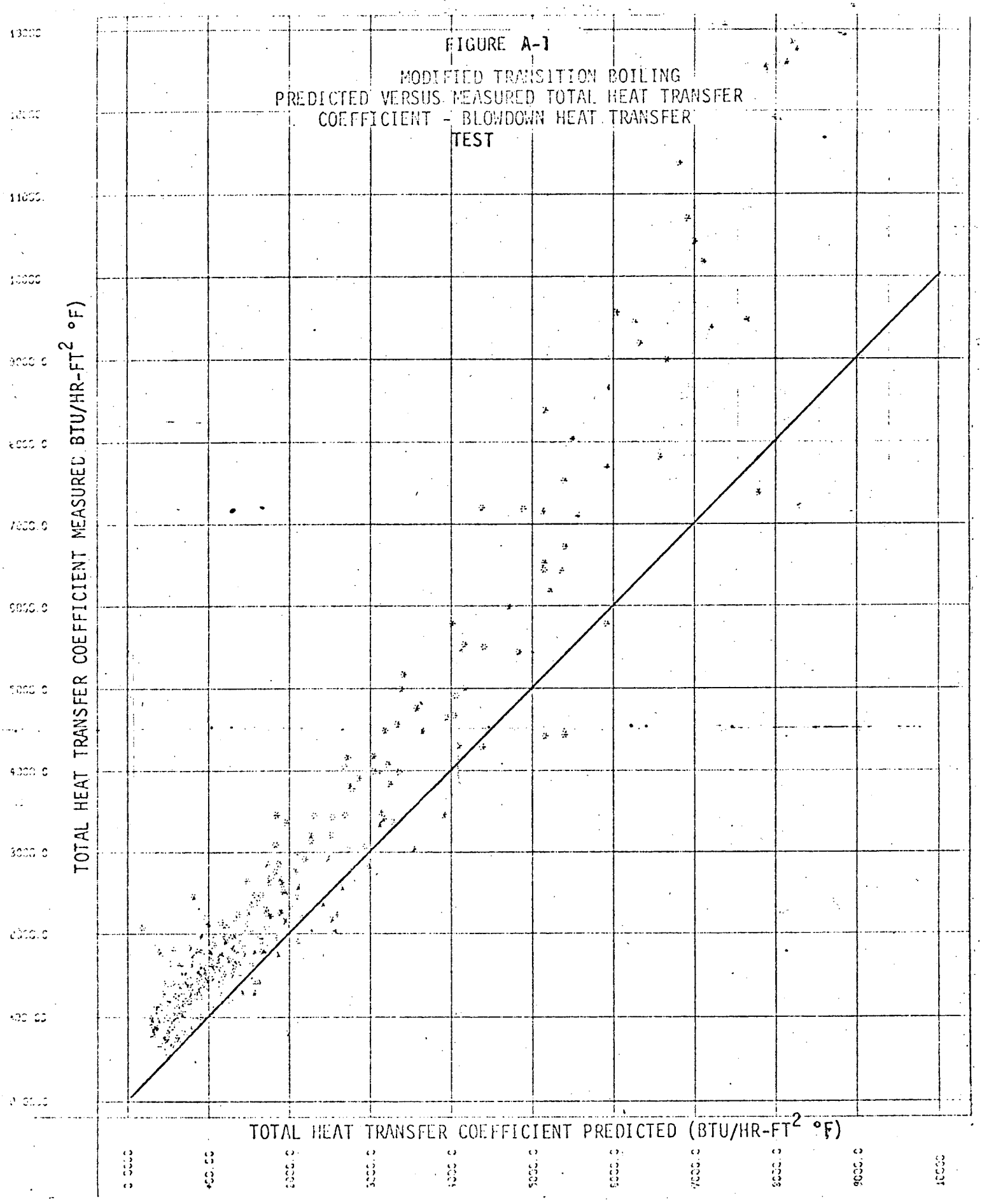
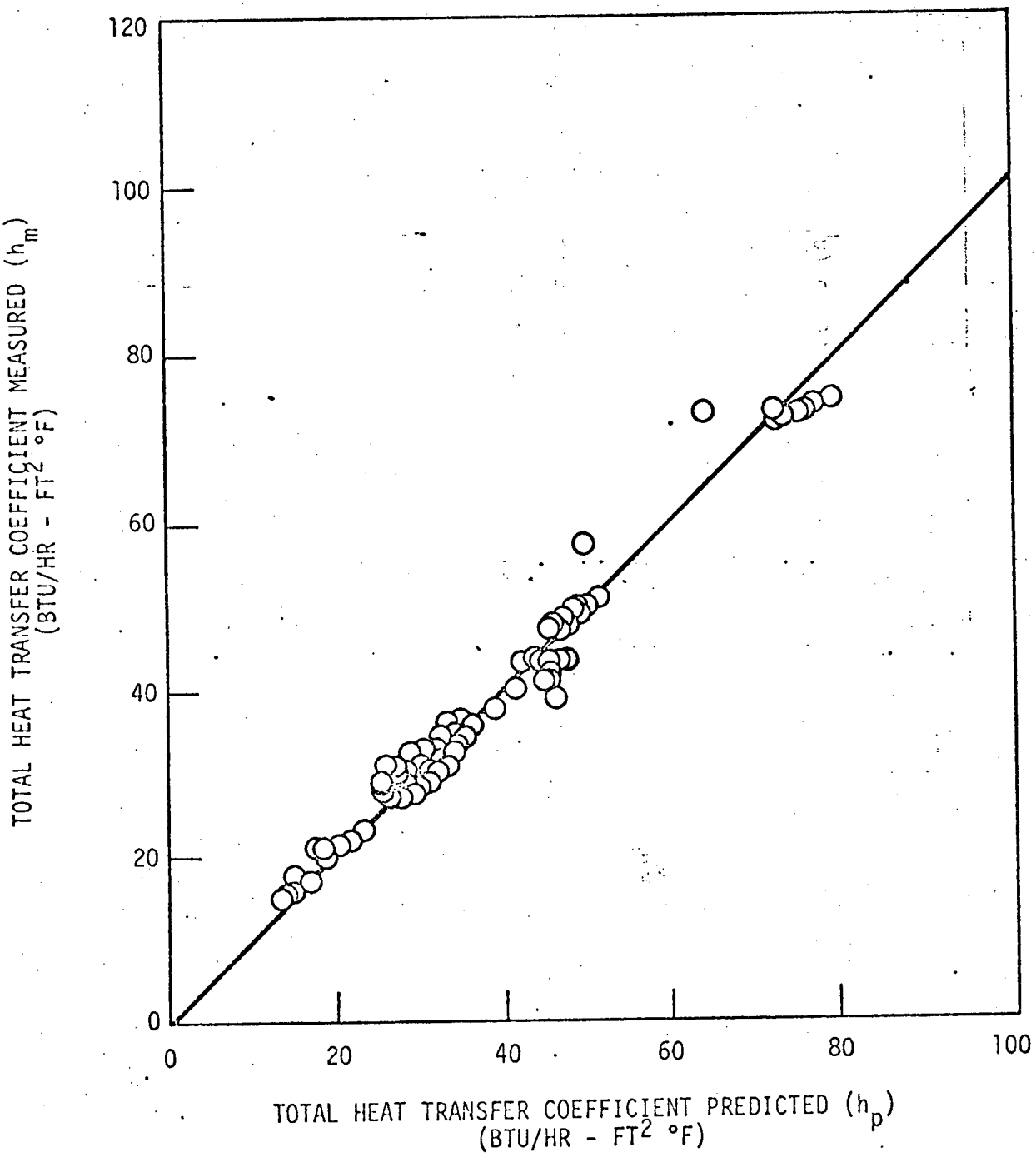


FIGURE A-1

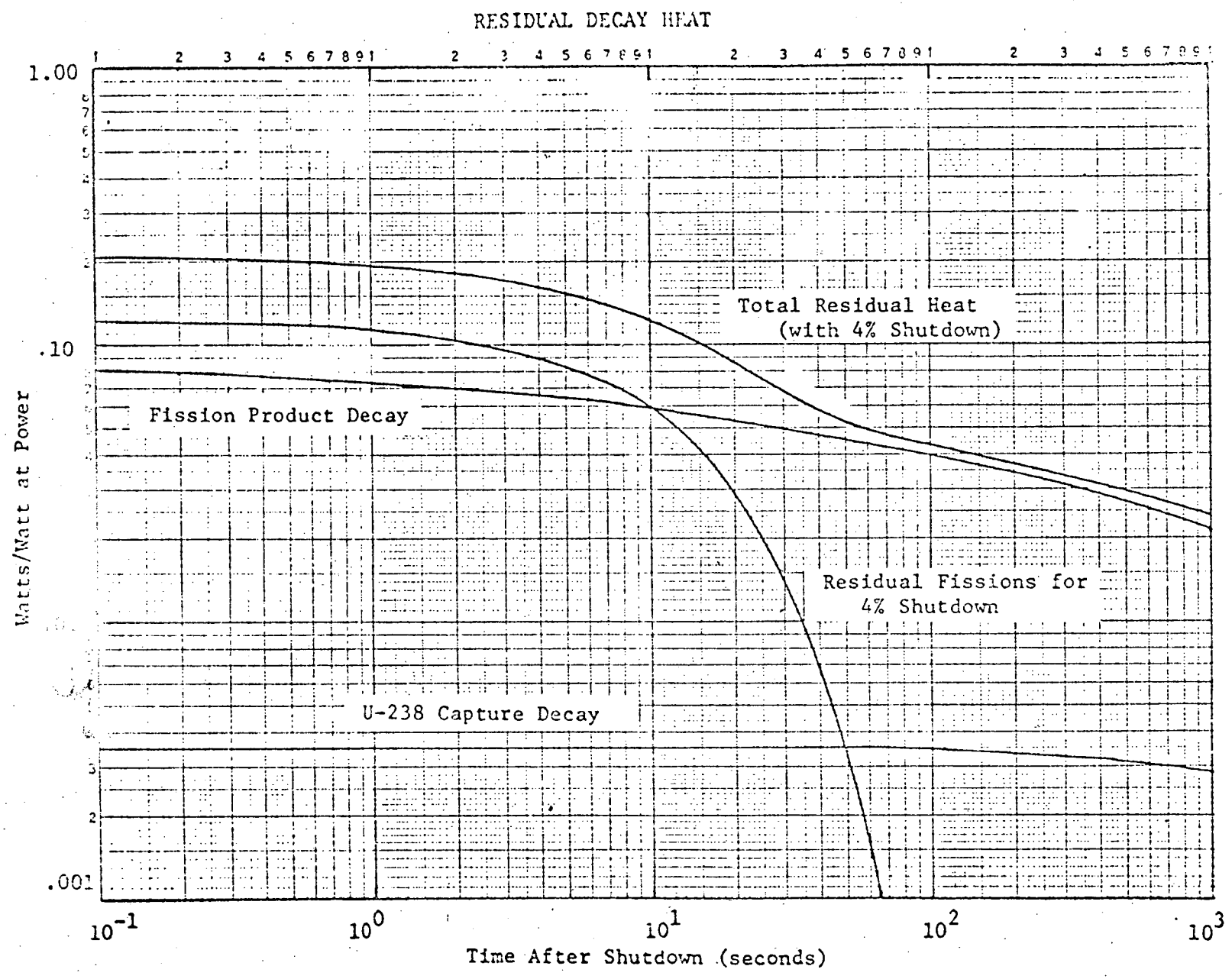
MODIFIED TRANSITION BOILING
PREDICTED VERSUS MEASURED TOTAL HEAT TRANSFER
COEFFICIENT - BLOWDOWN HEAT TRANSFER
TEST

FIG. A-2



UNIVERSITY OF MICHIGAN HEAT TRANSFER TEST - TURBULENT FLOW DATA - TOTAL HEAT TRANSFER COEFFICIENT - LOW PRESSURE HEAT TRANSFER TEST

FIG. B-1 ??


RESIDUAL DECAY HEAT
1.00
.10
.001
Watts/Watt at Power
Total Residual Heat
(with 4% Shutdown)
Fission Product Decay
Residual Fissions for
4% Shutdown
U-238 Capture Decay
10^{-1}
10^{0}
10^{1}
10^{2}
10^{3}
Time After Shutdown (seconds)

APPENDIX C

Response to letter from Division of Reactor Licensing dated July 7, 1971

By a letter of July 7, 1971 the Division of Reactor Licensing requested the Applicant to provide certain information required for its evaluation of the emergency core cooling system for Indian Point Unit No. 2. The following indicates the location in the preceding report of specific items of information requested, and in addition provides the requested information not covered by the report.

Item 1 (a) System Pressure Transients
Figures 6, 7, 8, 9

1 (b) Hot Spot Clad Temperature Transients
Figures 10, 11, 12, 13

Hot Spot Local Mass Velocity
Can be obtained by using 80 percent of the core flow on Figures 6, 7, 8, 9

Hot Spot Fluid Temperatures
Figures 18, 19, 20, 21

Hot Spot Heat Transfer Coefficient
Figures 6, 7, 8, 9

1 (c) Core Pressure Drop
Figures 14, 15, 16, 17

Hot Spot Quality
Figures 6, 7, 8, 9

Average Mass Velocity at Core Midplane
Figures 6, 7, 8, 9 (From Core Flow)

1 (d) Heat Flux Distribution in the Hot Channel was a 1.79 Peak to Average Cosine

1 (e) Flow Rates in Upper and Lower Plenums
Figures 22, 23, 24, 25

1 (f) Flow Rates in the Broken and Intact Cold-Leg and Hot-Leg Piping
Figures 26, 27, 28, 29

1 (g) Flow Rate Out of the Break
Figures 22, 23, 24, 25

1 (h) Hot Spot Local Clad Metal-Water Reaction
Table 2

Item 2 Description of the Core Reflood Model
Page 10 thru 17

Item 3 There were no deviations from Appendix A, Part 3 of the interim policy statement

The core cooling analysis was performed on the basis of a peak power density of 17.4 kw/ft at 102 percent power. The appropriate Technical Specification reference to power distribution will be revised as follows:

Page 3.10-2

Section 3.10.2.2(b)

The hot channel factors shall be determined and maximum allowable power shall be reduced one percent for each percent the hot channel factors exceed the design values of

$$F_q^N = 2.90 \quad \text{or} \quad F_{\Delta H}^N = 1.66$$