

**STUDIES
OF FAST REACTOR DISASSEMBLY
USING
A BETHE-TAIT COMPUTER CODE**

by

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ABSTRACT

A brief resume of the arguments in favour of the fast reactor is given, followed by a short description of general fast reactor design. The postulated faults leading to possible whole core involvement are divided into two broad categories, Loss of Flow and Transient Overpower. Two methods of analysing the potential consequences of such incidents, the probabilistic and the deterministic, are outlined. The deterministic approach is then covered in more depth. The course of an incident is commonly split into several stages, each usually treated by a separate computer code. These stages are: steady state initialisation, predisassembly, disassembly/transition phase and containment response. Only predisassembly and disassembly are considered further, as the other stages lie outside the scope of this study. Predisassembly codes are described in general, and several particular codes are examined in more detail, based on a literature survey. The results and implications of disassembly calculations using the code EXTRA are presented. Here, the effects of several factors, such as the presence of retained fission gasses and possible restraints on fuel motion, are investigated. Some comparisons are made with published results from the VENUS-II disassembly code. These two codes are described and compared. A general conclusion drawn from these studies is that under some circumstances, the yield predicted during disassembly is

relatively insensitive to modelling assumptions, and a simple code such as EXTRA may prove quite adequate if explicit core displacements are not required. Also, a major factor in determining the yield of the disassembly phase is confirmed as being the rate of reactivity insertion during disassembly, as predicted by a predisassembly code.

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LIST OF SYMBOLS

$N(t)$	Total reactor power at time t
ℓ^*	Neutron lifetime
$C_i(t)$	Density of i th delayed neutron precursor at time t
λ_i	Decay constant of i th delayed neutron group
β_i	Yield constant of i th delayed neutron group
$\beta = \sum_{i=1}^6 \beta_i$	Defined as β of reactivity
S	Neutron source term
$k(t)$	Net reactivity at time t
$Q(t)$	Total energy deposited in core since beginning of transient
$dQ(t)$	Increase in energy over previous timestep
$h(t)$	Input reactivity at time t
$f(t)$	Doppler feedback at time t
$g(t)$	Disassembly feedback at time t
$T(i,j,t)$	Temperature in mesh cell i,j at time t
$P(i,j,t)$	Pressure in mesh cell i,j at time t
$E(i,j,t)$	Energy density in mesh cell i,j at time t (Energy/unit mass)
$n(i,j)$	Time independent normalised power in mesh cell i,j (per unit volume)
$\rho_f(i,j)$	Constant smeared fuel density in mesh cell i,j
$\rho_T(i,j)$	Constant smeared total density in mesh cell i,j
$\rho_T(i,j,t)$	Variable smeared total density in mesh cell i,j at time t
$e(i,j,t)$	Specific energy in mesh cell i,j at time t (energy/unit volume)

$\Delta e(i,j,t)$	Increase in specific energy over previous timestep
$v(i,j,t)$	Specific volume of mesh cell i,j at time t
$\Delta v(i,j,t)$	Change in specific volume over last timestep
d	Doppler coefficient
$\phi(i,j)$	Neutron flux in mesh cell i,j in steady state
$M_f(i,j)$	Mass of fuel in mesh cell i,j
w_m^*	Doppler weighting factor for zone m
$\underline{u}(i,j,t)$	Displacement of mesh cell i,j at time t
$D(i,j)$	Time independent material reactivity worth
$V_T(i,j,t)$	Total volume of mesh cell i,j at time t .

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CHAPTER 1

INTRODUCTION TO FAST REACTORS

1.1 The Role of the Fast Reactor

The introduction of nuclear power some 25 years ago was hailed as the dawn of a bright new era of cheap, clean and plentiful power for everyone. A quarter of a century later, having recorded many successes and with a growing scarcity of oil and natural gas in sight, the nuclear industry finds itself under more pressure than ever, with the two keys to a successful nuclear future, the fast breeder reactor (FBR) and the reprocessing of spent fuel, under the heaviest attack.

The Department of Energy is satisfied that some form of nuclear power is a necessity in the United Kingdom. North Sea oil gives a breathing space to establish information and clarify options, not an excuse to delay decisions. One of the main decisions to be made is do we need the FBR and reprocessing, or can we get by without them?

The total world reserves and potential resources of U_3O_8 are estimated to be between 3.7×10^6 and 4.2×10^6 metric tons, assuming a forward cost of U_3O_8 of between \$30 and \$50 per lb. (1). This will have been consumed by the middle of the next century if there is no plutonium reprocessing and present trends are maintained. Once reprocessing is allowed, the production of electricity from a combination of thermal and fast reactors may keep rising, if a satisfactory breeding ratio can be achieved.

From this point of view both FBRs and reprocessing seem very desirable. On the negative side, it is said that their use will bring

about the so-called 'plutonium economy' and great problems with waste disposal. In fact, the quantities of plutonium involved external to the core are not that great, something under one ton per reactor per year. Plutonium production is of the same order in breeders and non breeders, but it is the breeder which, by using plutonium as a fuel alleviates problems thought to arise from plutonium build up. Much larger quantities of military weapons-grade plutonium are being handled without problems now, and there are many non-nuclear industrial processes which could make much easier and potentially more dangerous targets for terrorist attack. As for the waste problem, it has been estimated that if all the electricity, both domestic and industrial, consumed by one man during his lifetime were produced by nuclear power, the resulting long-lived active waste could be contained in a glass block about 4 inches in diameter and 1 inch thick (2). The small physical size of the problem means that very elaborate methods can be used to dispose of the waste effectively. Nuclear incineration, especially in a FBR, can greatly reduce the half-lives of the active waste (3). The real problem with waste from power generation lies not with nuclear power stations, but with the millions of tons of ash and other noxious chemicals released to the atmosphere by fossil-fuelled plants.

Having accepted that there is a real and pressing need to develop a viable commercial fast reactor, it is obvious that such a reactor must be designed to be as safe as is humanly possible, but still within the bounds of economic reality. The design of such a plant will provide three levels of protection against the unplanned release of radioactivity to the surroundings or even within the plant itself.

Level 1 consists of a technically sound design which results in high reliability and minimises the risk of accidents. This is done by the use of intrinsic design features such as redundancy, design for ease of testing and inspection, fail-safe characteristics and quality control during construction. Use is also made of intrinsic features of the reactor, such as the use of low pressure coolant with high thermal conductivity, the stability associated with a negative power coefficient, and the high thermal capacity of the coolant, in which natural convection is easily started. This allows decay heat to be rejected for several hours with little or no external power.

Level 2 provides protection against possible failure or malfunction in a manner that minimises plant damage and ensures safety of the public and operating staff. This involves the use of effective fault detection systems and multiple reactor shut-down systems. Both these systems must have adequate redundancy, diversity, inspectability, testability and a conservative design margin to ensure effectiveness and reliability.

Level 3 provides further protection capability in the plant to give additional assurances that protection to the public is provided even in extremely unlikely circumstances. Conservative assumptions and evaluation methods are used to ensure the integrity of the containment under extreme conditions, imposed internally through a major reactor fault or externally by conditions such as earthquakes, floods or even aeroplane impact. Even though such extreme conditions are never expected to occur during the lifetime of the plant, their consequences must be analysed. Here we shall only be concerned with the behaviour of the reactor after some internal fault under the extremely pessimistic assumption that all the shut-down systems fail.

In its normal operating condition, a fast reactor core is not in its most reactive state. A reconfiguration of the core into a more reactive geometry due to some mishap is mechanistically possible. Also, in many core designs, the voiding of coolant from the core centre has a positive reactivity effect, and again this is mechanistically possible. If either of these happens, and the reactor fails to trip, a potentially highly dangerous situation will have arisen.

Although such a combination is so unlikely as to be almost incredible - estimates run at less than one in 10^7 reactor years - level 3 of plant safety demands that such accidents be analysed and their effect on the containment structure assessed. This work hopes to illustrate how certain important stages of such an analysis are carried out and presents the results of such an analysis. Before the methods of analysis are described, a brief description of the main types of fast reactor will be given.

1.2 The General Design of Fast Power Reactors

There are three basic categories into which FBRs can be divided, depending on the choice of coolant. These are the Liquid Metal Fast Breeder Reactor (LMFBR), the Gas Cooled Fast Reactor (GCFR) and the Molten Salt Fast Reactor (MSFR). To date, the LMFBR has received the most attention, with only the LMFBR having reached commercial exploitation, while the GCFR and especially the MSFR remain in the planning or conceptual stages. In view of this, only LMFBRs will be considered here.

In the LMFBR, the coolant used is molten sodium, or sometimes a mixture of sodium and potassium. In spite of the obvious problems of

handling molten sodium, it is chosen as a coolant because of its excellent heat transfer and heat transport characteristics, and good nuclear properties. To remove the risk of interaction between active sodium and steam or water, primary and secondary coolant circuits are used. Only the secondary circuit passes through the steam generators. A reactor usually has at least three such sets of primary and associated secondary circuits to provide adequate cooling for the core. This also provides a large safety factor, should coolant flow in one circuit unexpectedly cease.

The secondary circuits, the core itself and the control equipment are similar in outline if not in detail for all LMFBRs, but there are two distinct ways of arranging the components of the primary circuit i.e. the intermediate heat-exchanger and pump, and the reactor containment vessel. These arrangements are the loop and the pool configurations, illustrated in figs. 1.1a and 1.1b respectively. In each case only one coolant loop is shown for simplicity.

There are many arguments for and against each design, some of which are outlined in Table 1.1. However, until much more operating experience has been acquired, there appear to be no major advantages or disadvantages on either side (4). This is borne out by the very even division between loop and pool types chosen for the reactors listed in Table 1.2.

As has already been noted, the general engineering design of a LMFBR core does not vary too much from reactor to reactor. The following description applies to a typical core of about 3000 MW thermal output. The fuel consists of a mixture of PuO_2 and enriched UO_2 , the mass of heavy metal (Pu+U) in the active core being around 20 metric tons. The active core is about 1m high and 1.4m in radius. The fuel is contained in thin stainless steel fuel pins which are themselves grouped into bundles enclosed by hexagonal stainless steel subassembly wrappers. There

are some 350 subassemblies each containing around 300 fuel pins. Above and below the active core regions the fuel pins contain depleted UO_2 to act as axial breeder blankets. There is also a radial breeder blanket made up of subassemblies containing fuel pins filled with depleted UO_2 . Within each pin, located above or below the axial breeder is a plenum to accommodate the fission gasses released during burn-up. The subassemblies are held by a diagrid, which is mounted on a strongback suspended from the roof of the reactor vault. A section of a fairly typical reactor is shown in Fig. 1.2 and an individual subassembly is shown in Fig. 1.3.

Obviously details such as fuel pin and fuel pellet diameters, numbers of fuel pins, numbers of subassemblies, location and size of the fission gas plenum, and so on, will vary from design to design, but the general outline presented will hold good. In Chapter 6, the results of the analyses of two different cores will be presented. The cores analysed are those of a fairly large pool reactor, core A, and a smaller loop reactor, core B. Table 1.3 gives some of the engineering details on which the models used in the analysis are based.

CHAPTER 2

MODES OF LMFBR FAILURE AND THEIR ANALYSIS

2.1 Modes of Failure

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Reference has already been made to the safety of the plant resting on three basic levels of protection. The faults against which this protection is provided can themselves be subdivided into several levels of seriousness, as indicated in Table 2.1. As can be seen, the possible faults range from those causing little or no effect to those causing gross disruption of the core geometry. Here we are only concerned with those faults in level 3, assuming the worst possible case that all control systems fail and the reactor does not trip.

The initiators of such accidents are many and various, depending on the individual reactor design, but nearly all of them cause either Loss of Flow (LOF) or Transient Over Power (TOP). One class of accident which does not fit neatly into either category is pin failure propagation from subassembly to subassembly, eventually leading to whole core involvement. In all three cases, the ultimate result is an unplanned addition of reactivity, possibly at a high rate, due to the relocation of fuel, cladding material or coolant. The effects of such movements are summarised in Table 2.2. As the reactor is assumed to remain untripped, the transient will continue until neutronic shutdown is achieved by some rearrangement of the core geometry. Whether this rearrangement is energetically benign or violent will depend on the detailed course of events.

2.1.1.1 Loss of Flow

Examples of events leading to a loss of flow type accident are: complete failure of on and off site power supplies to the coolant pumps, a frozen bearing in one or more pumps, or, in a loop reactor, a major break in one of the primary coolant pipes. An equally serious leak from the primary containment of a pool reactor is highly unlikely. Once coolant flow has started to slow down, a possible sequence of events might be as follows. Coolant boiling starts in the highest power subassemblies and slowly spreads outward. This voids coolant from the core, adding reactivity and raising the power level. Cladding in the first areas to boil dries out, begins to melt and may be swept out of the core by coolant vapour flow, adding still more reactivity. Fuel in the voided region starts to melt and slumps under gravity toward the core centre. By this time, the core is very near prompt critical. Fuel pins in the unvoided outer regions of the core may fail by internal pressurisation from pellet-clad interaction, releasing molten fuel into the coolant stream. A violent molten fuel coolant interaction (FCI) may ensue adding reactivity at a high rate and taking the core on a super-prompt-critical excursion, terminated by disassembly of the core under fuel vapour pressure. A more detailed description of this scenario will be presented in Chapter 4.

2.1.1.2. Transient Overpower

Transient overpower initiators are slightly more limited in scope, being restricted to direct causes of reactivity insertion. Examples of possible initiators are: unplanned withdrawal of control rods, compaction of the core due to seismic shock and passage of an entrained gas bubble

through the core. Once the reactivity has started to rise due to any of these initiators, a possible chain of events might be: the power rises, but coolant flow remain steady. The doppler effect slows down the rise in power as the fuel heats up, giving time for heat transfer to the clad and coolant. Eventually the clad begins to fail, either through differential thermal expansion, or internal pressure from fission gas released from restructured or molten fuel, or possibly by melting of the clad. Clad failure leads to either fission gas release or escape of molten fuel into the coolant stream. In either case, coolant is rapidly expelled, taking the reactivity well beyond prompt critical. As before, the excursion is finally terminated by core disassembly.

Loss of flow and transient overpower are generally recognized as being the chief paths to a possible core disruptive accident (CDA).

2.1.3 Pin Failure Propagation

During the normal operation of a plant, it is almost inevitable that at least a few fuel pins will fail. This may be due to manufacturing defects, incorrect loading (over enrichment) or small blockages leading to local overheating and failure. If such a failure can propagate in a cascade fashion to involve the whole subassembly and then spread further through the core, this would be a serious cause for concern. However, it is widely argued that such propagation is highly unlikely (6,7,8) or, if possible, will be limited to a small number of adjacent subassemblies (9), because axial propagation on the same scale combined with the governing dispersed flow regime will have led to subassembly venting and permanent

fuel dispersal into the upper liquid sodium plenum.

For such propagation, the affected subassembly must contain a large proportion of molten fuel, and for this to occur, it must be voided of coolant for an appreciable length of time. It has been demonstrated that neither the simultaneous release of fission gas from over 200 fuel pins (10) or the complete or partial blockage of up to 100 coolant subchannels (11, 12) will lead to the development of such conditions.

2.2 Methods of Analysis

The analysis of scenarios such as the ones presented above can proceed along one of two paths, the probabilistic or the deterministic.

2.2.1 The Probabilistic Approach

Many of the variations of this approach are to some extent based on the Farmer Safety Criterion approach (13). This aims to keep releases of radioactivity to the environment, usually measured in terms of release of iodine -131, below a criterion having the form of a release/frequency plot. A sample plot, used for SGHWRs is shown in Fig. 2.1 (after 14). The total iodine -131 inventory of any core is known, and this places a lower limit on the acceptable frequency of the events considered. From the analysis of the frequency of failure of reactor components and the probability of failure of sections of the protective systems to operate on demand, it is possible to identify those combinations of events which occur more frequently than this lower limit. The iodine -131 release

resulting from these faults can be assessed and the plant designed in a way that all such points lie within the design criterion.

The complete range of foreseeable fault conditions affecting the safety of the reactor has to be analysed. To simplify the analysis, use is usually made of fault diagrams. These start with an initiating event and follow all possible pathways to either a benign termination or a release of radioactivity. At each junction point, a reliability figure is assessed. Thus, knowing the possible frequency of the initiating event and the amount of activity released, the safety of the plant can be judged. Possible simple fault diagrams for LOF and TOP are shown in Figs. 2.2 and 2.3.

As there is insufficient data, experimental, operational or theoretical available on many of the events postulated to occur, no reliability figures are quoted. This is one of the main drawbacks of applying this method to such unlikely events. These two diagrams can themselves be regarded as sub-diagrams of much larger fault diagrams, beginning with earthquake or loss of off site power as an initiator. This is another drawback of fault diagrams, they tend to grow uncontrollably.

It is possible, by such splitting up of systems into their individual components, to draw very large trees, each leg of which may be well within the safety criterion. This can give an over optimistic view of a system which may in fact be barely adequate for duty. Various solutions to this problem have been proposed, most of them integrating the risk to give a Mean Annual Severity figure (15). This is the sum of the products of the release in curies and the frequency in years⁻¹ for all legs for a particular initiating event.

One of the major advantages of the probability approach is that it makes the designer look more deeply and logically at his design and

leads to a better understanding of the system . It allows for logical comparisons of alternative designs, as relative merits should show up even though the data base may not be good enough for absolute prediction. The probabilistic approach is of value during early design, as it can help to ensure economical design.

2.2.2 The Deterministic Approach

Here, the theoretical understanding of material behaviour gained through experiment (both laboratory and in-pile) and operating experience is used to trace the course of a transient in as much detail as possible. The severity of the transient is judged by the work-energy released. Such a treatment necessitates the use of large complex computer codes.

The limits to this approach are: the limited understanding of material behaviour under extreme conditions, limited computer storage space and running time, and limited experimental verification of code predictions. One result of this is that most codes tend to be parametric in nature i.e. they use empirical fits to experimental data rather than calculate values explicitly from first principles. This gives the codes more flexibility in areas of uncertainty. There also has to be a compromise between the detail required to adequately model individual phenomena, such as coolant boiling, and overall code size. The models used are nearly always much simpler than the best available 'state of the art' codes which have to be used in a 'stand alone' mode.

The course of a core disruptive accident (CDA) breaks down quite neatly into three or four phases, and nearly all computer codes used restrict themselves to a particular phase of the accident.

These phases are: predisassembly, when the reactor still has an intact core geometry; disassembly, when the engineering structure is deemed to have been lost; and containment response, which estimates the structural response of the reactor vessel, head and other components to the work energy deposited during the preceeding phases.

Results of calculations have shown the possible existence under some circumstances of a fourth phase, known as the transition phase (16, 17). In such a case the initiating transient is too mild for an energetic disassembly to occur, and the predisassembly phase terminates in the formation and growth of a boiling pool of fuel and steel in which a recriticality would be unlikely. There would be gradual fuel removal from the pool by ejection through limited openings above and below the pool. Ejected fuel would then be quenched by the liquid sodium and the resulting work energy would be negligible. Such a situation is extremely difficult to model analytically, and analysis of the transition phase is still at a very early stage. Both transition phase and containment response lie outside the scope of this work.

Predisassembly and disassembly will now be discussed in greater detail in the following Chapters.

CHAPTER 3

THE PREDISASSEMBLY PHASE

3.1 Predisassembly Codes

The spectrum of events covered by predisassembly codes is extremely wide. Initially the reactor is operating at some steady power level, usually taken to be normal full operating power. One of the events postulated in Chapter 2 is then assumed to occur and the ensuing course of events is followed. Calculation is terminated either when the reactor is neutronically shutdown whilst retaining a coolable core configuration, or when the core geometry is so distorted that the models used lose their validity. This would be the case for both the transition phase and for violent core dispersal.

An ideal predisassembly code would fulfil most of the following requirements. Extensive steady state calculations would be made to obtain selfconsistent values for: the initial power and flux distributions from the neutron transport equation, flow and temperature distributions from a thermo-hydraulic study, and fuel structure/fission gas release patterns corresponding to the assumed previous operating history. Once the transient had been initiated, the code would follow in detail the inter-dependent motions and behaviour of all core material throughout the transient. Taken into consideration would be:

Fuel: restructuring and further release of fission gas,
thermal expansion,
melting,
relocation of molten fuel or solid fuel debris within the
fuel pin or in the coolant channel and behaviour of released
fission gas.

Cladding: thermal expansion,
clad failure from whatever reason,
melting and relocation of molten clad or solid
clad debris.

Coolant: thermal expansion,
boiling,
thermal interaction with molten fuel or clad.

The power changes driving these events would be traced by solving the time dependent neutron transport equation at successive time steps, taking into account any superimposed reactivity changes and reactivity feedbacks due to motion of materials or thermal effects. The models used to represent core deformation would be flexible enough to follow the transient through to neutronic shutdown by whatever path.

As the core of an FBR contains some 10^5 fuel pins distributed amongst roughly 300 subassemblies, clearly it is not feasible to perform calculations for each pin, or even each subassembly. Solving the time dependent neutron transport equation also presents problems, as it is very complex and mathematically 'stiff', necessitating very small time steps in a numerical solution. Because of this, many simplifying assumptions have to be made to reduce the resulting program to a manageable size. Even so, some codes have running times in excess of 3 hours!

The main assumptions most commonly made are:

- i. The core may be subdivided into a number of channels (usually up to about 30). This subdivision can either be by subassembly ring or by grouping subassemblies with similar power, flow and burnup.
- ii. Each channel can be represented by a single fuel pin with its associated coolant flow area and heat sink, representing the

core structure. All fuel pins in all subassemblies making up the channel are assumed to behave in an identical fashion.

- iii. The reactor kinetics can be calculated using the point kinetics representation of the multigroup diffusion equations with reactivity worths calculated from first order perturbation theory.

The general effect of assumptions i and ii is to make the model behave in a much more coherent fashion than the real core. For example, it becomes almost impossible to model circumferentially incoherent fuel pin failure and pin-to-pin failure propagation. This restricts such codes to the study of LOF and TOP type incidents and may well lead to the calculation of artificially high rates of reactivity insertion and unduly severe accident consequences.

Setting aside the general doubts which have been cast on the use of point kinetics (19), assumption iii restricts codes using first order perturbation theory to fairly small material motions. This is one reason why they are unable to follow the transient into the transition or disassembly phases.

There are other, equally limiting assumptions built into the various models describing the physical processes taking place. These vary from code to code and depend on its prime objective. The British predisasassembly code FRAX will now be described to illustrate some of these assumptions and limitations. A survey of the more commonly used American and European codes is given in Appendix I. These descriptions do not necessarily give a correct picture of present-day code capabilities, as development work never ceases and published reports are usually a few years out of date.

3.2 FRAX-2

3.2.1 General Introduction

FRAX-2 is the latest version (20) of the U.K. whole core accident code. The layout of the code is shown in Fig. 3.1.

The converter edits tapes produced by the neutron diffusion code MARC together with raw input data to prepare suitable input data for both predisassembly and disassembly phases. This consists of the power profile, flux distribution and tables of fuel and coolant worth.

The predisassembly phase, which shall be described in some detail, is a much modified and improved version of the American code MELT-II. This writes to disc temperature and power distributions at a chosen switchover point, and a relevant reactivity history. The link then interpolates these onto the disassembly mesh for use together with the worth tables produced by the converter.

The disassembly phase is usually the U.K. code EXTRA, but the American VENUS code can also be used. These will be discussed in Chapter 5.

FRAX can deal with both LOF and TOP cases by the use of input tables of flow history or reactivity history.

3.2.2 Core Representation

The core is divided up into a number of concentric annular channels. Each channel contains one representative fuel pin. Each pin has: a coolant region (1 node), a clad region (2 nodes), a gap or bond region, a fuel region (up to 12 nodes) and a central hole. Up to twenty equal axial segments are allowed, representing the active core. There may be up to thirty channels, and each can contain a different type of pin.

The outer channel may represent a breeder region if desired, but then it will not be included in the calculation of core average temperatures. The axial breeder region is included in the hydraulics and coolant voiding feedback calculations but not in the heat transfer calculation. Any radial reflector is not represented.

The fuel pins have no interaction with each other in the temperature and hydraulics calculation other than through reactivity feedback effects on the ratings.

A schematic of the code representation of the core is shown in Fig. 3.2.

3.2.2 Cladding and Fuel Behaviour

The cladding is assumed to fail through melting, melt-through of molten fuel or excessive strain. Strains due to differential thermal expansion of fuel and clad and due to internal fission gas pressure are calculated. As long as the clad remains elastic, the gas pressure strains are ignored. Once the clad has become plastic, the strain due to gas pressure is still ignored until its rate of increase exceeds that of the differential expansion strain. When this occurs, the fuel-clad gap will open, so that no more differential strain can take place, and further increases in strain are calculated from the gas pressure. If the percentage of plastic strain exceeds a temperature dependent criterion or alternatively if the clad temperature exceeds a set value, then the clad is assumed to fail. In either case fission gas release is assumed. If the channel contains coolant, then coolant voiding is initiated. The fission gas pressure is calculated from the average burnup for the channel

and a simple temperature dependent fuel restructuring gas release model.

If at the time of clad failure the percentage of molten fuel in the pin exceeds a value dependent on the fission gas pressure, then fuel collapse is assumed rather than fission gas release. Modelling restrictions do not allow both to occur in the same channel simultaneously. Fuel may also collapse without clad failure if the percentage of molten fuel exceeds a set value (usually 75%) or if the fuel temperature exceeds a set value. Fuel is released into the coolant channel and falls under gravity. All sections above the failure point also fall under gravity or under the driving pressure of fission gas in the upper plenum. If fuel is released into a channel containing coolant, a fuel-coolant interaction (FCI) is assumed to start.

Thermal conductivity of the fuel is given as temperature dependent tabular input for each channel, whilst clad thermal conductivity is a constant for each channel. The clad may be bonded to the fuel or separated by a gap, with the appropriate bond conductance or gap coefficient. This gap coefficient is treated as a constant, although it varies by a factor of three or four with the fuel-clad gap size. Clad and fuel temperature are calculated at the top of each segment.

3.2.4 Coolant Behaviour - Voiding and FCI

In the hydraulics calculation, fairly simple standard hydrodynamics equations can be used. This is because in the hydraulics calculation the coolant density and channel mass flow rate are assumed constant. Detailed geometry is not specified beyond the ends of the fuel bundle.

A number of velocity heads are supplied for momentum losses above and below the core and also equivalent lengths for friction losses outside the fuel bundles.

The core outlet pressure is a specified datum point, and the inlet pressure is either also specified or calculated from the total pressure loss along the channel provided by the fluid flow calculation. The 'local' pressure at any point in between is obtained by linear interpolation between inlet and outlet pressures.

The different voiding mechanisms allowed are: boiling, fission gas release and FCI. Once voiding has started the coolant stream is divided into two slugs separated by a bubble of known internal pressure. The only difference in treatment between the voiding mechanisms is in the calculation of the bubble pressure.

Boiling is checked for by comparing the local saturation pressure with either the local or inlet pressures. An alternative check is if the local clad and coolant temperatures exceed set values. Once boiling has started, the bubble pressure is the local saturation pressure at the point of initiation.

Initiation of fission gas release and FCI has already been discussed. The initial volume of the fission gas is the volume of voids in the fuel and the initial pressure is the average pressure of the gas in these voids. Subsequently the pressure of the bubble is calculated from the volume such that $PV^\gamma = \text{constant}$.

The FCI process is split into three stages. Initially fuel is released into the coolant channel and displaces its own volume of coolant without heat transfer between them. After a set delay time, supplied as input data, heat transfer is allowed to take place between fuel and coolant in the Interaction Zone, and the bubble starts to expand at a rate given by standard thermohydrodynamics equations, taking

into account pressure waves reflected from the ends of the subassembly but neglecting friction of the sodium slugs. Once vapour starts to form in the bubble the slug stage is entered and heat transfer between fuel and coolant in the bubble is no longer explicitly calculated.

Voiding of a channel by one means precludes the others. However, FCI can start and take over in any channel providing there is coolant present at the point of failure.

It is also possible to artificially insert a bubble of any desired length moving at any desired speed at any desired time during the transient.

The heat transfer coefficient for heat transfer to the coolant is a constant. If the particular segment is voided, the appropriate film coefficient is used.

3.2.5 Neutron Kinetics and Reactivity Feedbacks

The standard point kinetics representation of the neutron diffusion equation is used, with up to six groups of delayed neutrons. At low reactivity or when the power is in balance with the neutron precursors, the numerical solution is obtained by the zero neutron lifetime approximation. At all other times a modification of Hansen's algorithm is used. During the course of the calculation the time step used can vary in length from 1s to 10^{-6} s. The size of the time step is regulated by temperature and reactivity changes, distance moved by coolant, and by the need to accurately resolve triggers for events such as coolant boiling or fuel collapse.

The reactivity variation driving the transient is the sum of externally supplied input reactivity and internally calculated feedbacks.

The input reactivity is entered as a reactivity-time library and may include a negative component to simulate a reactor scram. Feedbacks are obtained from the doppler effect, fuel slumping or expansion, changes in density of clad and coolant, and voiding of coolant channels.

The doppler feedback is a spatially weighted function of the temperature change since the start of the transient, taking into account changes in coolant density and coolant voiding. The reactivity effect of fuel collapse is obtained from input tables of fuel worth distribution. The same can be done for fuel thermal expansion. However, as the clad is much stronger, it is possible to assume the fuel and clad are locked together and to obtain the reactivity effect of fuel/clad thermal expansion from clad movement only. The coolant voiding reactivity feedback is calculated from tables of sodium worths, making allowances for the void fraction of the bubbles.

CHAPTER 4

PREDICTED BEHAVIOUR DURING PREDISASSEMBLY

4.1 Code Comparison

As can be seen from the previous chapter, from Appendix I, and from the precis of code capabilities given in Table 4.1, each code has its strengths and weaknesses. For example, whilst MELT has good fuel pin internal fuel motion modelling, good fuel failure correlation and large channel capability, it has no boiling model and only parametric models for FCI and external fuel motion. In general, the two American code systems described have more sophisticated models and treat a wider range of effects, such as the detailed fuel initialisation scheme in MELT or the cladding relocation model in SAS.

A major question to be answered is, at what stage do increased numerical accuracy and phenomenological realism cease to outweigh modelling complexity with its resulting high costs in computer storage and running time. It is very hard to give a definitive answer, as there are great uncertainties in the understanding of the physical processes involved and in the data bases supporting the models used.

The sequence of events postulated is so severe and so extremely unlikely, that it is impossible to check the overall predictions of the codes against experiment or operating experience. On the other hand, a good deal of laboratory and in-pile testing, such as the ANL TREAT series of tests, has been done on the behaviour of fuel pins and coolant on a part-subassembly or single-fuel-pin scale.

The results of the individual phenomenological¹ modules used in stand-alone mode have been widely checked against such experimental results. In the case of parametric² models, these are calibrated to reproduce observed effects. Generally, the models used describe observed behaviour fairly well, at least qualitatively speaking, though of course with varying degrees of accuracy from code to code. It is also a problem that in many cases the experiments do not fully represent prototypic conditions, and the results have to be extrapolated for transient operation. The field of detailed material behaviour is outside the scope of this study.

Of more interest here is a direct comparison of the predictions of the various codes for the same incident. This gives an indication of sensitivity to modelling complexity. Serious discrepancies in results would point to areas needing closer attention. Such comparisons have been carried out, in the States between SAS and MELT, and in Europe between the European Codes described in Chapter 3 and in Appendix I. This latter comparison has recently been extended to include SAS.

4.1.1 MELT III and SAS3A

This comparison was carried out at HEDL (21) in order to identify major discrepancies between the two codes. The incident analysed was a 50 Csec^{-1} unprotected transient overpower accident in the Beginning-

¹Phenomenological module: based on first principle analysis using known physical laws.

²Parametric module: based on empirical fit to experimental results.

of-Cycle-4 (BOC-4) core of the FFTF. Only two channels were used to represent the core, as this minimised computer running time but was still deemed sufficiently accurate to show up any areas of gross discrepancy.

Some differences were found in the basic physical data used as input for the SAS and MELT analyses. These reflect the differences in the models used.

The fuel/cladding gap conductance plays a large role in both steady-state and transient behaviour. It became obvious that the gap width and hence gap conductance were the primary controlling variables in establishing the steady-state and transient temperature profiles. MELT has better models for gap width and conductance, both completely determined from well established internal correlations included in the SIEX code, which is used as a MELT subroutine. These are based on extensive experimental results, whereas the SAS gap width and conductance model is basically parametric. It was assumed that the MELT results were correct, and much effort was expended to establish the SAS parameters which gave steady state results as consistent as possible with the MELT predictions. The resulting SAS and MELT steady-state temperature profiles and fuel restructuring patterns are shown in Figs. 4.1 and 4.2.

The small differences in the coolant temperature profile can be explained by the different handling of the coolant heat capacity. Differences in the inner clad temperature are caused by the use of a constant thermal conductivity in SAS and of a temperature dependent thermal conductivity in MELT. There is fairly good agreement on fuel outer surface temperatures when the SAS gap width and conductance predictions are changed to reflect MELT values. In such a case the codes

predict similar fuel centreline temperatures. The fuel restructuring patterns are also close.

The SAS pressure profile was adjusted such that the steady state pressure drop across each zone was as close as possible to the MELT prediction. This was done by changing the input hydraulic diameters.

The failure models available in SAS were not used, but the time and position of failure as predicted by MELT using the Damage Parameter failure criterion were supplied as input data. This was thought reasonable as the codes predicted essentially identical temperature distributions just prior to failure.

The reactivity traces for the first four seconds of the transient are shown in figs. 4.3 and 4.4. Prior to failure the traces are practically the same, though there is a slight difference of 1.0 - 1.5¢ in the doppler feedback which cannot be seen on the scale used. This is due to the slightly different temperatures calculated by the two codes.

After failure, the reactivity traces diverge somewhat. The SAS fuel motion feedback shows a succession of steps. During each of these, fuel squirting has stopped. As the interaction zone-coolant interface is maintained at the bottom of the fuel pin rupture, squirting quickly restarts as interaction zone pressure falls and the pin cavity pressure begins to rise again. In MELT, once the bubble has been swept out of the core at about 2.9 sec., there is no further activity until much later in the run when the power has built up again.

The difference in doppler feedback is probably mainly due to the different temperatures predicted, but the different relationships

used to adjust the doppler coefficient in voided areas may also have some influence.

The difference in sodium void feedbacks is due to the different treatments of sodium void worth distribution. MELT handles the sodium void and fuel worths from the bottom of the orifice region up to the surface of the sodium pool. SAS allows for no sodium worth above the upper reflector and thus neglects a substantially finite and negative reactivity contribution.

Before fuel failure, the power traces shown in Fig. 4.5 are very similar, though by the failure point the MELT power is about 100 M.W. higher. This is probably due to the cumulative effect of the small difference in doppler feedbacks showing up in the repeated integration of the point kinetics equations. The use of the zero neutron lifetime approximation is not thought to introduce any significant error. After failure, SAS has a higher net reactivity due to the different fuel motion and voiding feedbacks, and so has a higher power level. Once the continued squirting of fuel in SAS has brought the net reactivity down to the MELT level, the power traces become similar again.

The axial temperature profiles for coolant, clad inner surface, fuel outer surface and fuel centre, all at a time just before failure (2.68 sec., failure at 2.6955 sec.) are shown in fig. 4.6. Both coolant and inner clad temperatures are about 30° - 35°C lower throughout in SAS. Also, the SAS fuel outer surface temperature is higher than in MELT. This may well be due to the slightly higher gap conductances used in MELT. The 'bump' in both the SAS and MELT fuel outer surface temperature at high axial levels is caused by the fuel/clad gap being closed below this level (95cm) and open above in both codes. SAS used a single melting temperature (2767°C), producing the flat topped fuel

centre temperature profile, whereas MELT uses a range of temperatures from solidus (2760°C) to liquidus (2816°C). This gives the linear region in the fuel melting zone. Allowing for this, the fuel centre temperatures are essentially the same in both cases.

The author of the study concludes that MELT is easier to use, as the user is relieved of having to provide data for the calculation of fuel/cladding gap conditions, a critical area. This advantage is partially offset by the superior primary loop model in SAS. Although considerable differences in detailed behaviour, due to modelling differences in the fuel ejection schemes and the representation of the FCI zone, are noted, the overall behaviour pattern is essentially the same. Unfortunately the study is limited to the first four seconds of the transient, and the conditions at either a switch-over point to a disassembly phase or at a benign shutdown are not quoted. It is also a pity that the failure prediction models of the codes were not compared, as fuel failure prediction is another critical area in predisassembly analysis. In this case, the time and position of failure as predicted by MELT was used as input to SAS. These factors slightly mar an otherwise useful comparison.

4.1.2 CAPRI, FRAX, CARMEN, SURDYN and SAS

The comparison of the European codes (22) was carried out by the Whole Core Accident Codes group, a subgroup of the Safety Working Group of the Fast Reactor Coordinating Committee. The calculations were made for a fictitious reactor called the 'Europe' reactor. This

is a reasonably representative model of a fast reactor of about 2000 M.W. thermal output, but it does not represent the detail of any specific engineered reactor. Recently preliminary calculations were performed at ANL for the same benchmark cases on the 'Europe' reactor, using a version of the code SAS3D (23).

These two sets of calculations deal with both LOF and TOP cases. As the LOF results appear to be more complete, a brief resume of the main results and conclusions of this case only will be presented.

The 'Europe' reactor core consists of one central (withdrawn) control rod, surrounded by 216 driver subassemblies arranged in 8 concentric rows and 114 breeder subassemblies in two further rows. There are two enrichment zones in the active core. All the codes use a consistent core representation, in which each row of subassemblies is represented by one channel. The control rod is represented by one channel, channel 1, and both rows of radial breeders are also represented by one channel, channel 10, except in SURDYN which uses two channels for the breeders, and CARMEN which does not represent the breeders at all.

An extract of the steady state results is given in Table 4.2, and the steady state axial distributions of coolant temperature and average fuel temperature are shown in Fig. 4.7. The calculations are based on a steady state linear heat rating of 450 w/cm in the peak power node. The small (less than 2%) differences in overall reactor power are due to differences in the axial mesh definitions and in the condensation of the power distribution from the neutron diffusion mesh to the core nodes. The coolant outlet temperatures agree well. As CAPRI allows for inlet orificing in all channels to maintain consistent inlet and outlet conditions, and FRAX and CARMEN do not, the steady state channel mass flow rates calculated by CAPRI were used as input to FRAX

and CARMEN. SAS and SURDYN performed their own mass flow rate calculations, SAS in a similar fashion to CAPRI and SURDYN directly from the channel dependent power generation for the specified uniform coolant temperature rise.

The pressure drop along the core is predicted fairly consistently but outside the core there is wide disagreement. This is certainly due to the use of different hydraulic representations of the primary coolant loop outside the active core.

The axial temperatures show close agreement in both fuel and coolant. SAS has the highest average fuel temperature and CARMEN the lowest, but the difference is less than 4%. Radial steady state temperature profiles (not shown) are in even closer agreement. The small differences seen arise from inconsistencies in radial and axial mesh divisions, detailed heat transfer treatments and averaging procedures.

The transient part of the analysis is based on a flow rundown with a halving time of six seconds. As in the steady state, CAPRI has the most consistent and detailed thermohydraulic treatment of the flow rundown of the European codes. The CAPRI flow rundown for each channel is specified in terms of the time dependent inlet pressure, and fully accounts for the various channels being orificed to the same outlet temperature. This is physically more correct than using the same prespecified flow rundown for each channel. The expression chosen for the inlet pressure results in a very close approximation to the desired flow rundown, however after about 4 sec. the specified flow falls off slightly from the CAPRI value. For consistency, the flow rundown curves calculated by CAPRI are used as input to FRAX and CARMEN. The SAS flow rundown curve agrees with the CAPRI value to within 1%. SURDYN, which does not simulate the acceleration of the sodium slugs

during coolant boiling, uses the originally specified flow decay.

Some of the more important results of the transient phase are compared in Table 4.3. The relative power traces and the reactivity traces are shown in Figs. 4.8 and 4.9 respectively.

The onset of boiling is spread over $1\frac{1}{2}$ seconds, though the relative power at this time is roughly the same. SURDYN, having the slightly faster original flow rundown, boils first. The other differences in boiling initiation are mainly due to different boiling criteria. FRAX is restricted to initiate boiling at the top of the active core otherwise boiling occurs much earlier, at 14.78 sec in the upper breeder. Because of the single bubble model used, this bubble would be rapidly swept away causing little reactivity disturbance.

The main driving force for the transient is the voiding reactivity, and the influence of this can be seen in Figs. 4.8 and 4.9. SURDYN has a very simple voiding model in which the voiding reactivity from a channel is introduced as a constant ramp, starting immediately the channel begins to boil. This rapidly raises the power and speeds up the attainment of boiling conditions in the other channels. FRAX, CARMEN, CAPRI and SAS have increasingly complex boiling/voiding models. This seems to increase the incoherence of the voiding pattern, which is well predicted by all the codes, and tends to reduce the voiding velocity. This causes longer delays between the onset of boiling and the start of the reactivity feedback, and reduces the vigour of this feedback. The neglect of channel orificing and the resulting preferential downward voiding in FRAX and CARMEN contributes to the higher voiding ramp rates predicted by these codes.

The total void reactivity released is fairly consistently predicted and all voiding ramp rates are positive, though their magnitudes vary by a factor of up to 2. Despite similar steady state temperatures and identical crossover mean temperatures, there is quite a variation in doppler reactivity and ramp rate. This is due to differences in the handling of sodium-in and sodium-out doppler coefficients, and in the temperature calculations.

The net reactivity at crossover is very consistently predicted to be about 98¢, but, reflecting differences in voiding and doppler ramp rates, the net ramp rates vary enormously. The ramp rate transferred to the disassembly phase, which calculates its own doppler feedback, is known as the driving ramp rate. This is the net rate less the doppler rate, and ranges in value from 15.2s^{-1} (SAS) to 36.2s^{-1} (FRAX). This obviously affects the outcome of the disassembly phase, but a combination of this, the different normalised power levels at crossover, the different methods of calculating reactivity feedbacks, and the exponential nature of the relationship between fuel vapour pressure and internal energy all tend to reduce the differences in the energy deposited at the end of the disassembly phase. The energy deposited by the crossover point is fairly consistent at around 40000 M.J.

In this study, the conditions were deliberately chosen to guarantee that the accident would proceed to disassembly. In a more realistic case, finer channel-to-subassembly resolution, axial expansion and the release of fission gas from restructuring burnt-up fuel would all be allowed for. These would all tend to decrease the coherence of voiding and prolong the duration of the transient. This would give the modelling differences even more chance to show up. In spite of this,

the authors consider that all the codes give reasonably reliable results for a case within their common limits, and that none of them contain gross errors placing the results of one code far outside the range of the others. The observed differences in results can, qualitatively at least, be explained by the differences in modelling.

CHAPTER 5

THE DISASSEMBLY PHASE

5.1 Disassembly Codes

Once conditions in the core resemble those described in Table 4.3, i.e. power several hundred times normal and rising, reactivity near prompt critical and rising with a part of the core molten or melting, the models used to describe core behaviour in pre-disassembly codes begin to lose their validity and have to be replaced. This is the present-day role of the disassembly code, although they have been, and indeed still are, widely used in a 'stand alone' mode for extensive parameter surveys. Because of their relatively short running times, they are much more suited for this purpose than pre-disassembly codes. They can thus give a feel for which parameters are of importance, and these can then be investigated in more detail for specific cases using the larger codes.

In most disassembly codes, the fairly detailed channel and fuel pin model used in the predisassembly phase is replaced by a model in which the core is represented as a homogenous mixture of fuel and structural material, with appropriate smeared densities. This mixture is acted upon by the vapour pressure of the molten fuel until enough fuel dispersal has taken place for the nuclear reaction to have been shut down. Apart from the programmed reactivity input, the only other reactivity feedback is taken from the doppler effect. Continued fuel slumping, coolant voiding and so on are usually allowed for through the reactivity input function. This reactivity input function is normally the reactivity history of the predisassembly phase

during the time after the switch over point. Once the power burst is over, most codes calculate the work energy released when the vapourised core expands adiabatically to some chosen final state. This is frequently taken to be 1 atmosphere as this allows easy comparison between different cases and codes.

Many of the codes used today are improvements of the original Bethe-Tait approach (24). By making a series of simplifying assumptions about reactor physical behaviour and geometry, they obtained an analytical method for calculating the nuclear energy released during the burst. The main assumptions they made were:

1. The neutronics can be adequately represented by point kinetics, ignoring delayed neutrons and the doppler effect (this last was reasonable for the metallic fuelled reactor they were modelling).
2. The reactivity worth profile and the power profile remain independent of time.
3. Displacements are small enough to allow first order perturbation theory to be used for worth calculations.
4. Material density remains constant.
5. There is no pressure generation until a threshold energy is exceeded, thereafter the energy-pressure relationship is linear.
6. Pressure wave effects are ignored.
7. There is no heat transfer between fuel and other materials.
8. The reactor has spherical geometry.

Over the years, nearly all these restrictions have been lifted or eased, which has meant that an analytical solution is no longer possible. In all cases advanced numerical solution techniques have to be used. A brief survey of a few early codes is given in Ref. (25), whilst more modern codes, such as FX2POOL (26), have reached the

stage of having detailed three fluid hydrodynamics in a two dimensional Eulerian mesh with heat transfer between fuel and structural materials, and neutronics handled by a space-time kinetics approach. The equations of state used have also been greatly improved, such as the density-dependent equation of state developed by Roberts (27).

The following sections will describe the main features of two commonly used codes. These are the U.K. code EXTRA and the U.S. code VENUS-II. Although neither of these can be regarded as a 'state of the art' code, both are sufficiently widely used, especially VENUS-II, for them to be thought of as fairly typical examples of disassembly codes. As these codes are quite well documented, only their main features and the differences in approach will be discussed.

5.2 General Description of EXTRA and VENUS-II

EXTRA (28) is a two dimensional disassembly code developed in the U.K. The core is modelled in R-Z Eulerian geometry and the core materials are treated as a homogenous non-compressible mixture. Material displacements are not explicitly calculated, and the material motion feedback is obtained from the virtual accelerations caused by the build up of fuel vapour pressure. The core is split up into several zones, which represent areas of different enrichment in the active core and also the axial and radial breeder blankets. Each zone has associated smeared fuel and total densities (constant), and a density independent equation of state. This links the local energy density to the local temperature and pressure.

Other improvements on the Bethe-Tait model are the inclusion of delayed neutron and doppler effects, and the approximation of reflected pressure waves. The assumptions of point kinetics, perturbation theory reactivity worth, no heat transfer from fuel to structural material, and the time independence of the worth and power distributions are retained. At the end of the power burst, the core is allowed to expand adiabatically to some chosen final pressure. The excursion yield is calculated as the energy released during this expansion.

VENUS-II (29) is an American disassembly code written at the Argonne National Laboratory. In terms of modelling assumptions, the main difference from EXTRA lies in the treatment of the hydrodynamics and the calculation of the disassembly feedback. Here the core displacements are calculated explicitly by solving the hydrodynamics equations in R-Z Lagrangian coordinates. These displacements are then used to calculate the material motion feedback. The equation of state used is necessarily density dependent. As in EXTRA the core is divided up into zones, each of which may have its own equation of state. Again, point kinetics are used and the power and reactivity worth distributions are assumed to be time independent. There is no heat transfer between fuel and structural materials, and no built-in calculation of excursion yield.

5.3 Mathematical Formulation of Code Models

5.3.1 Neutron Kinetics

Both codes calculate the transient reactor power from the point kinetics equations, using six groups of delayed neutrons. The

equations may be written:

$$\frac{dN(t)}{dt} = \frac{(k(t) - \beta)}{\ell^*} N(t) + \sum_{i=1}^6 \lambda_i C_i(t) + S \quad 5.1$$

$$\frac{dC_i(t)}{dt} = \frac{\beta_i}{\ell^*} N(t) - \lambda_i C_i(t) \quad 5.2$$

In EXTRA, the initial conditions are:

$$\frac{dN(0)}{dt} = \frac{dC_i(0)}{dt} = 0$$

i.e. $C_i(0) = \frac{\beta_i N(0)}{\lambda_i \ell^*}$

This exists as an option in VENUS-II, although usually the precursor densities are read in as input data obtained from a predis-assembly code.

In both codes, the energy deposition is taken as the integral of the power i.e. total energy deposited is:

$$Q(t) = \int_0^t N(t) dt \quad 5.3a$$

and energy deposited over the previous time step is:

$$dQ(t) = \int_{t-1}^t N(t) dt \quad 5.3b$$

The net reactivity is the sum of three components, the input reactivity, the doppler feedback and the disassembly feedback:

$$k(t) = h(t) + f(t) + g(t) \quad 5.4$$

In both codes the same options for the form of $h(t)$ exist.

The input reactivity may either be in the form of a quadratic equation:

$$h(t) = A + Bt + Ct^2 \quad 5.5$$

in which case the constants A, B and C are supplied, or it may be input as a time history in tabular form. In both codes the neutron source term is usually set to zero.

5.3.2 Calculation of Local Pressure and Temperature

5.3.2.1 EXTRA

In EXTRA, the temperatures and pressures are related to the local energy density via equations of the form:

$$T(i,j,t) = F_T(E(i,j,t)) \quad 5.6a$$

$$P(i,j,t) = F_P(E(i,j,t)) \quad 5.6b$$

These relationships are supplied to the program as tabular input. The local energy density is obtained from the total energy by the relationship:

$$E(i,j,t) = \frac{n(i,j)}{\rho_f(i,j)} Q(t) \quad 5.7$$

The temperature and pressure at any point in the core can then be found by interpolation in the appropriate table.

5.3.2.2 VENUS-II

As the equations of state are density dependent, the relationships are rather more complex. The change in local specific energy is given by:

$$de(i,j,t) = \rho_T(i,j,t) \left[\frac{n(i,j)}{\rho_T(i,j,0)} dQ(t) - P(i,j,t-1) dv(i,j,t) \right] \quad 5.8$$

The first term is equivalent to eqn. 5.7, and the second term is the PdV work energy arising from the change in mesh cell volume. The total specific energy at that point is obtained by summing these changes, making allowance for the heat of fusion. This is then substituted into a set of complicated analytical fits to the corresponding states data of Menzies (30). These expressions are known as the ANL Equation of State, and take the form of:

$$P = \text{MAX}(P_v, P_\ell) \quad 5.9$$

where:

P = fuel pressure

$$P_v = \exp \left[A - BT^{-1} - C \ln T \right]$$

$$P_\ell = \begin{cases} D \cdot F & \text{for } V_r < 1 \\ G(E - H - IV_r) & \text{for } V_r > 1 \end{cases}$$

T = fuel temperature

$$= J \cdot E - K$$

E = fuel internal energy

V_r = reduced specific volume of fuel

$$= \frac{\rho_c}{\rho_f}$$

ρ_f = fuel density

ρ_c = critical fuel density

$$D = E - L + MV_r + NV_r^{-3}$$

$$F = P \exp \left[-QV_r + RV_r^2 \right]$$

$$G = S / (YV_r - W)$$

$A, B, C, H, I, J, K, L, M, N, P, Q, R, S, Y, W$ = constants

5.3.3 Doppler Feedback

5.3.3.1 EXTRA

The doppler feedback is assumed to have a simple exponential relationship to the change in temperature from the beginning of the transient. On integration this becomes:

$$f(t) = d \ln \left[\frac{\bar{T}(t)}{\bar{T}(0)} \right] \quad 5.10$$

The temperature used is the flux squared weighted average temperature:

$$\bar{T}(t) = \frac{\int_V \phi^2(i,j) T(i,j,t) dV}{\int_V \phi^2(i,j) dV} \quad 5.11$$

Here, $\int_V$ indicates integration over specified core zones e.g. just active core zones.

5.3.3.2 VENUS-II

VENUS-II uses a slightly more complicated relationship between reactivity effect and temperature change. The contribution from the mth zone is:

$$f_m(t) = -2a\bar{T}_m(t)^{-1/2} + b \ln \left[\bar{T}_m(t) \right] + \frac{c}{d} \bar{T}_m(t)^{d-1} \quad 5.12$$

Usually the constants a, c and d are set to zero, reducing this to the same form as eqn. 5.10:

$$f_m(t) = b \ln \left[\bar{T}_m(t) \right] \quad 5.13$$

In this case, a mass weighted average temperature is used:

$$\bar{T}_m(t) = \frac{\int_m T(i,j,t) M_f(i,j) dV}{\int_m M_f(i,j) dV} \quad 5.14$$

$\int_m$ indicates integration over zone m. The overall doppler feedback is then a weighted summation of the zone contributions:

$$f(t) = \sum_{m=1}^M \left[f_m(t) - f_m(0) \right] W_m^* \quad 5.15$$

The weighting factors reflect the differences in zone volumes and relative reactivity importance. They are defined such that:

$$\sum_{m=1}^M W_m^* = 1 \quad 5.16$$

5.3.4 Disassembly Feedback

5.3.4.1 EXTRA

The equation of motion of the molten fuel under the driving pressure of the fuel vapour pressure can be written as:

$$\frac{\partial^2 \left[\rho_T(i,j) \cdot \underline{u}(i,j,t) \right]}{\partial t^2} = - \nabla P(i,j,t) \quad 5.17$$

As the density is assumed to remain constant, this can be rewritten as:

$$\frac{\partial^2 \underline{u}(i,j,t)}{\partial t^2} = - \frac{1}{\rho_T(i,j,t)} \nabla P(i,j,t) \quad 5.18$$

The material reactivity worth is defined as the reactivity effect of adding unit volume of material at a given point. Thus the total reactivity effect of material motion throughout the core is given by:

$$g(t) = \int_V \underline{u}(i,j,t) \cdot \nabla D(i,j) dV \quad 5.19$$

where $\int_V$ indicates integration over the whole core volume.

As the reactivity worth is assumed independent of time, we can differentiate eqn. 5.19 twice w.r.t. time to get:

$$\frac{d^2 g(t)}{dt^2} = \int_V \frac{\partial^2 \underline{u}(i,j,t)}{\partial t^2} \cdot \nabla D(i,j) dV \quad 5.20$$

Substituting from eqn. 5.18 into eqn. 5.20 we have:

$$\frac{d^2 g(t)}{dt^2} = - \int_V \frac{1}{\rho_T(i,j)} \nabla P(i,j,t) \cdot \nabla D(i,j) dV \quad 5.21$$

This expression can be integrated twice to obtain the reactivity feedback $g(t)$.

Allowance for pressure wave effects is made in the following way. As the reactor is split up into M homogenous zones, eqn. 5.21 becomes:

$$\begin{aligned} \frac{d^2 g(t)}{dt^2} = & - \sum_{m=1}^M \frac{1}{\rho_T(i,j)_m} \left[\int_{V_m} \nabla P(i,j,t) \cdot \nabla D(i,j) dV \right. \\ & \left. + \int_{S_m} P' \nabla D(i,j) dS \right] \end{aligned} \quad 5.22$$

$\int_{V_m}$ and $\int_{S_m}$ indicate integration over the volume and surface area of zone m respectively. P' is the pressure perturbation due to the reflection of pressure waves from the zone boundaries. It is calculated from:

$$P' = p - P \quad 5.23$$

where P is the surface pressure in zone m and p is the interface pressure between zone m and an adjacent zone, $m + 1$:

$$P = \frac{P_m \rho_{T_{m+1}} C_{m+1} + P_{m+1} \rho_{T_m} C_m}{\rho_{T_m} C_m + \rho_{T_{m+1}} C_{m+1}} \quad 5.24$$

where C_m is the relative acoustic speed in zone m .

5.3.4.2 VENUS-II

The motion of the reactor material is explicitly calculated by the numerical solution of a set of two dimensional hydrodynamics equations. These assume that the core materials can be treated as a homogenous, nonviscous compressible fluid. The equations solved are those for the conservation of mass and the conservation of momentum.

As the Lagrangian mesh used deforms along with the fluid, the conservation of mass equation is easily written as:

$$\rho_T(i, j, t) = \rho_T(i, j, 0) \frac{V_T(i, j, 0)}{V_T(i, j, t)} \quad 5.25$$

The conservation of momentum equations are:

$$\frac{d^2 r(i, j, t)}{dt^2} = \frac{1}{\rho_T(i, j, t)} \frac{\partial P(i, j, t)}{\partial r} \quad 5.26a$$

$$\frac{d^2 z(i, j, t)}{dt^2} = \frac{1}{\rho_T(i, j, t)} \frac{\partial P(i, j, t)}{\partial z} \quad 5.26b$$

where r and z are the Eulerian coordinates of the Lagrangian mesh cell i,j at time t . These equations are the same as eqn. 5.17 with allowance for the time variation of the total density.

The return of reflected pressure waves is taken into consideration in the pressure term, $\underline{P}$, which is of the form:

$$\underline{P}(i,j,t) = P(i,j,t) + q(i,j,t) \quad 5.27$$

$P(i,j,t)$ is the pressure in the mesh cell obtained from the equation of state (eqn. 5.9), and $q(i,j,t)$ is the von Neumann and Richtmeyer (31) pseudoviscosity term, defined as:

$$q(i,j,t) = \left[\begin{array}{ll} \frac{1.44A(i,j,t)\rho_T(i,j,0)}{\dot{v}(i,j,t)} \left(\frac{\partial v(i,j,t)}{\partial t} \right)^2 & \text{for } \frac{\partial v}{\partial t} < 0 \\ 0 & \text{for } \frac{\partial v}{\partial t} > 0 \end{array} \right] \quad 5.28$$

where A is the surface area of the particular mesh cell. q becomes large in mesh cells undergoing rapid compression, which has the effect of smearing the shock wave over a finite distance. This increases the numerical stability of the solution of eqn. 5.26, and is in fact a standard way of facilitating this solution.

The accelerations are used to calculate new velocities, and these in turn are used to calculate new positions. Once the new mesh cell positions have been obtained, the cell densities are adjusted using eqn. 5.25. The boundary conditions used in the solution of these equations are radial restraint along the core centre line and a free boundary at the outer edges.

The reactivity effect of material motion is calculated from an equation similar to eqn. 5.19. Allowing for the time dependence of the density, and assuming that the reactivity worth remains constant,

the reactivity feedback associated with the calculated displacements, Δr and Δz , is:

$$g(t) = \int_V \frac{\rho_T(i,j,t)}{\rho_T(i,j,0)} \left[\frac{\partial D(i,j)}{\partial r} \Delta r(i,j,t) + \frac{\partial D(i,j)}{\partial z} \Delta z(i,j,t) \right] dv \quad 5.29$$

The values of the worth gradients are calculated at the centre of each mesh cell. They remain constant and continue to be associated with the same mesh cell as it becomes displaced.

5.3.5 Calculation of Excursion Yield

5.3.5.1 EXTRA

At the end of the transient, the total energy released during the transient, $Q(t_f)$, is known as the thermal yield. This energy is assumed to be contained in the single phase, saturated liquid fuel.

The local conditions are:

$$\text{Local energy density } E_1(i,j,t_f) = E_1^L \quad (\text{from eqn. 5.7})$$

$$\text{Local entropy } S_1(i,j,t_f) = S_1^L$$

$$\text{Local temperature } T(i,j,t_f) = F_T(E_1^L)$$

$$\text{Local pressure } P(i,j,t_f) = F_P(E_1^L)$$

At the end of the adiabatic expansion, the local conditions are:

$$\text{Vapour fraction} = \Delta(i,j)$$

$$\text{Local energy density } E_2(i,j,t_f) = \Delta(i,j)E_2^V + (1-\Delta(i,j))E_2^L \quad 5.30$$

$$\text{Local entropy } S_2(i,j,t_f) = \Delta(i,j)S_2^V + (1-\Delta(i,j))S_2^L \quad 5.31$$

$$\text{Local temperature} = T_2 \quad \text{uniform}$$

$$\text{Local pressure} = P_2$$

The superscripts V and L refer to fuel vapour and liquid respectively.

As the expansion is assumed to be adiabatic, there is no change in entropy, and the vapour fraction can be calculated from:

$$\Delta(i,j) = \frac{S_1^L - S_2^L}{S_2^V - S_2^L} \quad 5.32$$

The change in entropy between a liquid and vapour at a given temperature is:

$$\delta S_{VL} = S_2^V - S_2^L = \frac{L_2}{T^2} \quad 5.33$$

where L_2 is the latent heat of vapourisation at temperature T_2 .

The change in entropy of a liquid at different temperatures is:

$$\begin{aligned} \delta S_{LL} &= S_1^L - S_2^L \\ &= \int_{T_1}^{T_2} \frac{dE}{T(E)} \\ &= \int_{E_1}^{E_2} \frac{dE}{F_T(E)} \end{aligned} \quad 5.34$$

Once δS_{VL} and δS_{LL} are known, the vapour fraction can be calculated from eqn. 5.32. The change in local energy density during the expansion is then:

$$\begin{aligned} dE(i,j) &= E_1(i,j,t_f) - E_2(i,j,t_f) \\ &= E_1^L - \left[\Delta(i,j) E_2^V + (1-\Delta(i,j)) E_2^L \right] \end{aligned} \quad 5.35$$

The total excursion yield is the sum over the whole core of all the local contributions:

$$Q_{\text{rel}} = \int_V dE(i,j) \rho_f(i,j) dv \quad 5.36$$

where $\int_V$ indicates integration over the whole core volume.

5.3.5.2 VENUS-II

VENUS-II appears to have no built-in calculation of excursion yield at the end of the transient. However, subroutines which calculate the work done on isentropic expansion to some final pressure and which can be coupled to VENUS-II if desired, are available (32).

5.4 Program Methods

5.4.1 EXTRA

A simplified block diagram of the program is shown in Fig. 5.1. The blocks do not correspond to subroutines within the program, but represent the various stages passed through during a run.

The preliminary data consists of, amongst other things, the core geometry specification, flux and adjoint flux distribution, macroscopic capture and fission cross-sections and neutron group velocities. These are used to calculate the worth distribution, the normalised power distribution and the neutron lifetime. Once this has been done, data for the specific case is read in. This includes the reactivity input function, the doppler coefficient, the equation of state tables, the initial power, the point kinetics parameters, the condition for ending the transient and parameters defining conditions

at the end of the final adiabatic expansion.

A table of total energy values is set up, from zero to some input maximum. For each value of total energy, the program then calculates values of $\bar{T}$, f and $\frac{d^2g}{dt^2}$ as indicated in the previous sections. The kinetics equations are now solved using the Risley library Runge-Kutta predictor-corrector subroutine HAIR. At each time step the power is integrated to get the total energy, and the reactivity feedbacks are obtained by interpolation in the f and $\frac{d^2g}{dt^2}$ tables.

The transient is terminated when the power falls below some fraction, usually 10%, of its initial value. The program now sets up a table of energy density values, from E_2^L to some input maximum value. For each value of energy density, eqn. 5.34 is solved using a Simpson's Rule integration technique. δS_{VL} is supplied as an input data, and so the vapour fraction can be found from eqn. 5.32. This is then used in eqn. 5.35 to obtain the change in energy density for each value in the energy density table. Eqn. 5.7 is now used to calculate the local energy density at every point in the core, and the corresponding changes in energy density are obtained by interpolation in the table. These are summed to give the excursion yield - eqn. 5.36.

There is an option, known as the YIELD option, which allows various thermal yields to be supplied as input data. The excursion yield for each input thermal yield is calculated.

In all subsequent cases, only those tables affected by the new input data are recalculated. This can result in large time saving when parameter surveys are being carried out. As a fairly substantial, and fixed, part of the running costs comes from the calculation of worth and power profiles, it is much more cost effective to run the program for at least several cases at one time.

5.4.2 VENUS-II

A simplified block diagram of the program is shown in Fig. 5.2. As before, this does not represent individual subroutines.

The input data is divided into three categories. These are:

- i. Core dependent data including: core geometry and mesh cell dimensions, equation of state for each core zone, normalised power distribution, material reactivity worth distribution, doppler coefficients and weighting factors, point kinetics parameters, initial densities and volume fractions.
- ii. Case dependent data including: initial power and delayed neutron precursor density, initial temperature distribution.
- iii. Parameters controlling code operation including conditions for terminating the transient.

As can be seen, the normalised power and worth distributions are read in directly, instead of being internally calculated from flux distributions as in EXTRA. The initial temperature distribution is usually obtained from a predisassembly calculation. If this is not available, the code can estimate a distribution based on the normalised power distribution.

During the solution of the kinetics equations, the reactivity feedbacks for each time step are estimated by extrapolating forward from the known values for the preceeding three time steps. The extrapolation takes the form:

$$g(t) = A_1 + A_2 t + A_3 t^2 \quad 5.37a$$

$$f(t) = A_4 + A_5 t + A_6 t^2 \quad 5.37b$$

The constants A_n are obtained by fitting eqn. 5.37 to the $g(t)$'s and $f(t)$'s of the preceeding three time steps. This avoids

iteration between the power and feedback calculations, and is thought to introduce no significant error (33). Once the kinetics equations have been solved using these predicted feedbacks, the actual feedbacks for that time step are calculated. As this method is not self starting the program contains two methods (user input or internal estimation) of supplying suitable values of A_n for use in the first two time steps.

The calculation of the material displacements at each time step involves an iterative loop between the density dependent equation of state (eqn. 5.7) and the new density obtained at the end of the hydrodynamics equation (eqn. 5.25). Another iterative loop exists in the equation of state itself. Here, under some circumstances, e.g. in the single phase domain, the density becomes pressure dependent resulting in a set of coupled transcendental equations. This comes about because of the compressibility of sodium and steel in the core.

The transient is usually terminated when the power exceeds a lower or upper limit, though other termination criteria can be used. These include: limitation on mesh cell distortion, lower limit on multiplication factor, limitation on number of time steps.

The iteration necessary in the equation of state and between the equation of state and hydrodynamics, coupled with the enforced repeated solution of the hydrodynamics equations results in VENUS-II being a slower running code than EXTRA. Presumably because of this, it is set up to consider only one case at a time. No strictly comparable figures are available, but as a rough guide a typical $100s^{-1}$ ramp with 500 mesh points takes about 2 mins. CPU time on an IBM370 for VENUS-II (34). Some cases can take up to 30 mins. CPU time (29). A $100s^{-1}$ ramp with 891 mesh points and with eight different doppler coefficients takes just over 1 min. of CPU time on a CDC6600 using EXTRA.

5.5 Output Capabilities

5.5.1 EXTRA

Before the start of the transient, the flux squared and material reactivity worth distributions can be printed, together with the power distribution corresponding to the initial power level. The case dependent input data is then output. If desired, the energy dependent tables of $\bar{T}$, f and $\frac{d^2g}{dt^2}$ are also printed.

During the transient, at time intervals chosen by the user, the following information is printed: time into transient, multiplication factor, reactor power, total energy deposited since start of transient, delayed neutron precursor concentration, disassembly reactivity, doppler reactivity, input reactivity and net reactivity.

Once the transient is over, the energy density dependent tables of vapour fraction and change in energy density on expansion can be output before the excursion yield is calculated and printed. Finally, before the data for the next case is read in, the distributions of energy density, temperature and pressure corresponding to the thermal yield can be printed.

Amongst the input data are parameters which accurately control the amount of output produced by each case. The excursion yield is the only item printed unconditionally. All other output can be entirely suppressed or curtailed as required. Usually a full listing of input data and $\bar{T}$, f , $\frac{d^2g}{dt^2}$ tables is only required for the first case of a run. In subsequent cases only those data items which differ from the previous case need be printed. In most cases, the final distributions of energy, temperature and pressure are not required and can also be suppressed.

When using the YIELD option, these distributions corresponding to the input thermal yield are calculated and can be printed if required. This is a way of tracing the build up of energy density, temperature and pressure during a transient, when the total energy at various times is already known, say from a previous run.

5.5.2 VENUS-II

Initially, a documented edit of all the input data is produced. During the transient, the following data is printed after every time step: the time step number, the time into the transient, the energy in the molten fuel, the reactor power, the total energy, and the net, doppler and displacement reactivities. Further information, which is too bulky to be printed at every time step, is available at user specified intervals. This includes: radial and axial displacements, radial and axial velocities, total and viscous pressures, overall cell densities, and temperatures for all positions in the core. These are shown on a labelled R-Z grid. This data can be in high or low accuracy form. The high accuracy form is bulky and hard to read. The low accuracy version is much easier to read and more compact, but is limited to three figures of fixed point accuracy.

The program also prepares data files which can be passed to external graphical plotting routines. If these routines are not available, or not required, they must be replaced by dummy routines. The graphical output available consists of time histories of: power, energy, net, doppler and disassembly reactivity, and temperature and pressure at up to 10 specified mesh points.

As much more data is available for output than in EXTRA, for example the axial and radial displacements and velocities, the output from a typical VENUS-II case is much bulkier than from a corresponding EXTRA case. The full output from a VENUS-II run, including input data, can be in the region of 4000 lines, excluding graphical output (29).

The full unrestricted output for a comparable EXTRA case will almost certainly be less than 1000 lines.

5.6 General Validity of Assumptions

The basic assumptions common to both codes, and their justifications are:

- i. The reactor materials behave like a homogenous mixture with the properties of an isotropic non-viscous fluid.

This assumption is best satisfied for fairly severe transients. As the rate of increase of pressure and temperature rises, and more and more of the core structure is lost early on in the transient, the behaviour of the core materials more closely approaches that of the assumed homogenous mixture. In very mild transients where much of the core structure may still be intact, this will not be the case. Both codes try to make an allowance for this by allowing the user an option to in some way restrict the axial movement of the fuel.

- ii. Fuel vapour pressure (together with, in the case of VENUS-II, compression of the core materials) is the only source of internal driving pressure for the displacement calculations.

This neglects any effects from fission gasses retained in the fuel, or from sodium vapour generation - FCI. These can be to some extent estimated in both codes by modifications or additions to the equations of state used. As EXTRA assumes constant fuel density, it cannot treat the pressure changes associated with the change from single phase to two phase, as mesh cells expand or contract. On the whole, the result should be an overestimation of the pressure, as no pressure relief due to cell expansion is allowed.

- iii. Heat transfer effects are ignored.

Again, this assumption is best satisfied in severe transients. The time scale of these is so short that heat transfer effects should be small. In mild transients, perhaps ending in the transition phase, this would certainly not be the case. No allowance for this is available in either code without reprogramming to include some representation of an energy sink.

- iv. The reactor power can be calculated using point kinetics, and the spatial power density distribution remains constant.
- v. The reactivity change caused by material displacement can be calculated using first order perturbation theory, and the reactivity worth distribution remains constant. In VENUS-II the spatial worth gradients distort with the mesh.

These last two assumptions are in some ways the hardest to justify. In general, point kinetics can provide accurate results if the neutron flux shape remains fairly constant and the appropriate

shape function is used in calculating the reactivity and the feedbacks. As the displacement necessary to terminate the power burst is in many cases quite small (less than 5cm), the spatial flux shape ought not to be greatly distorted, and point kinetics may give reasonable results.

On the other hand, cases have been reported (35) where the worth net reactivity is much less than that calculated by more accurate means. It is 'folk wisdom' that worth reactivities are accurate up to one dollar, but the proofs of this are not always entirely convincing (19) and this need not always be the case. However, it is probably true to say that provided the worth is calculated using the flux distribution at crossover from predisassembly rather than the steady state flux, material displacements are small, and the transient is fairly severe so that material motion is homogenous and rapid, point kinetics and worth reactivity can give acceptable results. Also, in a severe transient, even if there is an error in the magnitude of the reactivity, the reactor will still shut down on a faster time scale than that of the increase in energy, thus reducing errors in yield.

CHAPTER 6

PREDICTED BEHAVIOUR DURING DISASSEMBLY

6.1 Introduction

In this chapter the results of computer studies of two LMFBR cores using the code EXTRA will be presented. Here, EXTRA is used in the 'stand-alone' mode. In part, the results presented form an extension of the parameter survey reported in an earlier study (25). There is also an investigation into the effect on disassembly of several features thought to be of importance. These include the roles of fission gas pressure and residual subassembly wrapper strength, both of which may have a significant impact on the course of the transient. Finally there is a comparison of results obtained from EXTRA with published VENUS-II results for similar cases.

The computer predictions were obtained by running EXTRA either on the CDC 6600 at the University of London Computer Centre or on the CDC 6500 at the Imperial College Computer Centre. The version of EXTRA used is basically the one described in Chapter 5, though as noted later, the input and output subroutines were extensively rewritten to handle different forms of input and to provide more output data. Before the results obtained are presented, a description of the core models used will be given.

6.2 Core Models Used

The engineering data on which the two core models are based was given in Table 1.3. Of course, in EXTRA such fine detail is not

represented and the core is assumed to be a homogeneous mass of fuel/steel mixture. The code representations of the two cores with the zones and meshes used are shown in Figs. 6.1 and 6.2. The corresponding dimensions and smeared densities are given in Tables 6.1 and 6.2, and the basic neutronic data in Table 6.3.

The data for core A was taken from data tapes supplied to the College as part of the EXTRA package (36), and represents the core of a large commercial pool reactor. The data for core B represents the core of a much smaller demonstration loop reactor, and was taken from the input to a published VENUS-II case (37). The fact that one core is from a pool reactor and the other from a loop reactor is of no importance in this analysis as EXTRA does not model anything outside the core and is only concerned with the behaviour of the core itself.

A slight problem arose because the input requirements of EXTRA and VENUS are not the same. The main difference lies in the way the worth and normalised power distributions are obtained. EXTRA calculates these from input neutronic cross-sections and flux distribution, whilst VENUS has both of them input directly. Thus the input subroutines of EXTRA had to be completely rewritten to accept this form of input. The resulting distributions of normalised power and material worth are shown in Figs. 6.3 and 6.4 respectively.

The normalised power distribution in terms of 'per unit mass' of core A is only about 1/2 that of core B. As these distributions are related to the inverse of the masses, this is to be expected, as the cores have a mass ratio $\frac{M_A}{M_B} \approx 2$. However, the initial power ratio $\frac{PO_A}{PO_B}$ is about 3, implying that core A should have higher initial power density, and this is indeed the case, the ratio of initial power

density being around $3/2$ as expected. Core B also has a higher worth distribution than core A. As the core is smaller, it is to be expected that the movement of a mass of material would have a larger reactivity effect than the same movement in a larger core.

The lack of flux values for core B also meant that the EXTRA flux squared temperature weighting had to be replaced by the VENUS mass weighting scheme. The average temperatures used for the two cores are shown in Fig. 6.5. Also shown is the mass weighted temperature calculated for core A. This always lies within 100K of the flux squared weighted temperature, and at low energies is very much closer. Figs. 6.6a and 6.6b show the variation of the reactivity feedbacks with energy for the two cores. Again, the flux squared and mass weighted doppler feedbacks for core A are within a few cents of each other. The similarity observed between the flux squared and mass weighted temperatures and doppler feedbacks gives confidence to the comparison of feedbacks between the two cores, and implies that the differences in the weighting functions disappear in the averaging process. For both doppler and disassembly feedbacks, core A requires about twice as much energy as core B for the same reactivity effect. This is in good agreement with the relative sizes of the cores.

At the same time, the output subroutines were changed to calculate and display more output. The additional output now available is the overall vapour fraction at the end of the excursion and expansion, the volume occupied by the core at the end of the expansion, the distribution of vapour fraction at the end of the expansion, and the flux squared or mass weighted average temperature at the end of the excursion but before the expansion.

6.3 Standard Result Set

The standard result set is an extension of the author's parameter survey previously mentioned (25). This was the investigation of the effects of reactivity ramp input rate and doppler coefficient on the excursion yield. The survey has been broadened to include a range of ramp rates from $5s^{-1}$ to $10000s^{-1}$ and a range of doppler coefficients from -0.003 to -0.0065 for both cores. The resulting excursion yield curves for core A and core B are shown in Figs. 6.7a and 6.7b respectively. The very high ramp rates shown are included for the sake of completeness, but they are physically unrealistic. Unfortunately, very few results were obtained for ramp rates below $5s^{-1}$. At such low ramp rates the transients become very long and prohibitively expensive in terms of computer units.

As can be seen, for the range of doppler coefficients considered, the relationship between doppler coefficient and excursion yield is almost exponential at very high and very low ramp rates. Here the yields increase steadily with ramp rate, whilst at medium ramp rates, from about $50s^{-1}$ to $200s^{-1}$ the situation is much more complex.

This phenomenon has been observed many times before (e.g. 38, 39) and is due to the relative changes in magnitude of the input ramp rate and the reactivity feedbacks. Briefly, at medium ramp rates and high values of doppler coefficient, the doppler feedback and input ramp rate combine to give several reactivity peaks and hence several power peaks before disassembly. A fairly typical example, with a ramp rate of $100s^{-1}$ and doppler coefficient of -0.005 is illustrated for both cores in Fig. 6.8. This shows the reactivity, power and energy traces during the transient. For low doppler coefficients, and for

all values of doppler coefficient at high and low ramp rates, only one reactivity and one power peak are observed. The way the number of power peaks changes with ramp rate and doppler coefficient is shown in Table 6.4. Now as the excursion yield is a sensitive function of the thermal yield, shown in Fig. 6.9, and the thermal yield is just the integral of the power over the course of the transient, these changes in the number and relative magnitude of the power peaks will have a large influence on the yield.

The exponential nature of the overall trend, especially at high ramp rates where the duration of the transient seems to be only a function of ramp rate, can be explained as follows. Ignoring delayed neutrons, the kinetics equation, eqn. 5.1, can be rewritten as:

$$\frac{dN(t)}{dt} = \frac{\Delta k}{\ell^*} N(t) \quad 6.1$$

where $\frac{\Delta k}{\ell^*}$ can be expressed as:

$$\frac{\Delta k}{\ell^*} = f(d, h, g, \ell^*, \beta) \quad 6.2$$

Ignoring heat transfer effects we again have

$$N(t) = \frac{dQ(t)}{dt} \quad 6.3$$

Hence the kinetics equation can be rewritten

$$\frac{d^2 Q(t)}{dt^2} - f(d, h, g, \ell^*, \beta) \frac{dQ(t)}{dt} = 0 \quad 6.4$$

With the initial conditions $Q = 0$ at $t = 0$, this has the solution:

$$Q_{\text{Final}} = D \exp[f(d, h, g, \ell^*, \beta) t] \quad (D = \text{constant}) \quad 6.5$$

The straightness of the yield curves in Figs. 6.7a and 6.7b implies that under certain conditions i.e. at very high and to a lesser

degree very low ramp rates, the governing parameter in the function $f(t)$ above is the doppler constant d . Hence the expression for yield can be simplified to:

$$Q_{\text{Final}} = D \exp(Ed) \quad (E = \text{constant}) \quad 6.6$$

Although the two cores generally behave in a very similar fashion, detailed differences do exist. Core A gives higher excursion yield and power levels, as is to be expected from a larger core with a higher total power. However, as shown in Fig. 6.10, core B has higher average specific excursion yields¹. This is expected because of the difference in power and mass ratios, but the difference in yields is greater than predicted. Assuming that energy is deposited in the same ratio as the initial power levels, the ratio of average specific yields should be 1.57 (A/B). In fact, this ratio ranges from 0.45 to 0.81, indicating that relatively more energy is deposited in core B than in core A.

Returning to the example of Fig. 6.8, and replotting the power history in terms of relative power P/P_0 as in Fig. 6.11a we find that core B indeed has higher relative power levels and hence relatively higher energy deposition than core A. The changing ratio of energy deposited is shown in Fig. 6.11b. The reason for this is to be found in the relationships of the reactivity feedbacks to the energy deposited, previously shown in Figs. 6.6a and b.

Initially, before feedback becomes significant, the relative power rises at the same rate in both cores with energy being deposited in the ratio of the initial power levels, 3.23. From Fig. 6.6a it is clear that such an energy ratio gives core A a larger doppler feedback than core B. As a result, the net reactivity and hence relative

¹Average specific yield - excursion yield divided by mass of active core.

power begin to drop below the levels of core B. Eventually reactivity is being extracted fast enough in both cases to cause the net reactivity and hence relative power to fall. Because of this difference in doppler feedback, core B reaches a higher peak net reactivity and higher peak relative power. The second peak follows basically the same pattern, with core B reaching higher net reactivity and relative power level. The disassembly reactivity in core B comes in sooner and at a higher rate than in core A, causing the net reactivity to fall more quickly and resulting in the lower relative power level seen in Fig. 6.11 towards the end of the transient. Again, this is the result of the different disassembly characteristics shown in Fig. 6.6b and the relatively higher energy deposited in core B. The energy ratio required for the same disassembly effect is roughly 2.2, whilst the actual energy ratio at the beginning of disassembly is 1.8 thus giving core B much higher rates of reactivity extraction by disassembly.

This is a specific example of a general trend in the behaviour of the two cores and is not confined to this one case. In general, the peak net reactivity and peak relative power in core B are higher, due to the later appearance of the doppler feedback in core B. The disassembly feedback starts sooner and has a higher rate in core B, because of the relatively larger amount of energy deposited by that time. The final excursion yield of core A is nearly always higher than that of core B, but because of the higher relative power in core B, the ratio of excursion yields is always less than the initial power ratio.

The development of power and energy with time during the transient has already been shown in Figs. 6.8c and 6.8d for a specific case. The way this influences the build up of temperature and pressure

in the core is shown for core A for the same case in Figs. 6.12 and 6.13 respectively. Figs. 6.12a and 6.13a show the rise of temperature and pressure with time at two points in the core. One of these, mesh point (1,15), is at the centre of the active core and has the highest power density. The other, mesh point (1,8), is at the centre of the upper edge of the active core. Figs. 6.12b, 6.12c, 6.13b and 6.13c show the radial and axial distributions of temperature and pressure at intervals during the transient. These figures are slightly distorted, as the radial and axial mesh points are shown to be evenly spaced. The true spacing was shown in Fig. 6.1.

The first power peak occurs whilst the core is still entirely solid, with peak fuel temperatures of around 2100K. Fuel first starts to melt after about 20.5ms, at the core centre, soon after the power has started to rise again. Fuel vapour pressure generation now begins in those meshes affected. By 22ms it can be seen that a small central region has completely melted and that a larger region around this, about half the active core volume, is undergoing phase change. All of these meshes are contributing to the vapour pressure, though as yet the peak fuel vapour pressure is only 0.2 bar. Once a mesh has melted, the local temperature and pressure rise very rapidly until power generation virtually ceases after the second power peak. By 26ms, 3ms after the second power peak, the entire inner enrichment zone and most of the outer enrichment zone are molten or melting. The peak fuel temperature is about 5600K and the peak pressures at the centre of the core driving the disassembly have risen to around 200-300 bar.

The overall distributions of pressure in the core for four different values of excursion yield are shown in Fig. 6.14. Once again the meshes have been represented as being of equal size.

The values of excursion yield considered are 1 G.J., 2 G.J., 5 G.J. and 10 G.J. In all these cases almost the entire inner enrichment zone and an increasing fraction of the outer enrichment zone are seen to contribute to the fuel vapour pressure, and hence are either entirely molten or are in the process of melting. The 'islands' of higher pressure building up in the outer enrichment zone are caused by the 'ears' on the normalised power distribution, shown in Fig. 6.3.

In general, core B behaves in a very similar fashion. Because of the relatively higher energy levels reached, local energy densities can be higher, giving higher values of peak and average temperatures and peak pressures. This is illustrated in Table 6.5.

Much of the following work had already been done for core A before EXTRA was set up to model core B. Due to the overall general similarity in behaviour observed, it was not thought necessary to completely duplicate this work with core B. Although much of the following relates only to one or other of the cores, the general trends predicted hold true for both.

6.4 Reactivity Input

6.4.1 Amount of Reactivity Inserted

All the ramp rates considered so far have been assumed to continue right to the end of the transient with, in effect, an unlimited amount of reactivity being available. In some cases well over \$20 had been inserted by the end of the transient. As, for example, voiding all the coolant from the core will only add about \$8, such high totals are physically unrealistic.

A closer study of the reactivity histories of the cases considered in Section 6.3 showed that the amount of reactivity inserted by the final power peak was roughly constant, with an average value of \$2.35. This is shown in more detail in Table 6.6. It appears that once disassembly has started, this proceeds so quickly that further reactivity addition is irrelevant. To confirm this, the $\$100s^{-1}$ ramp discussed in Section 6.3 was repeated with reactivity levelled off at \$3.0, \$2.5, \$2.275, \$2.0, \$1.5 and \$1.0. The results of this in terms of excursion and thermal yields are given in Table 6.7, and the power and energy histories are shown in Figs. 6.15a and 6.15b.

With the reactivity limited to \$3.0 and \$2.5, there is no significant difference from the unlimited case. In the \$2.275 case, reactivity levels off just as the final power peak is reached. The power history is indistinguishable on the scale used from the unlimited case, but it does fall off a trifle more quickly to give an excursion yield of 72% of the base case. In the \$2.25 case, the reactivity levels off after the final reactivity peak, but before the final power peak. This slightly reduces the peak power as can be seen, and with it the thermal yield. The resulting excursion yield is 64% of the base case. Below \$2.25, the original final reactivity peak is never reached and power levels are drastically reduced, with equally drastic effects on the energy levels reached. At these low levels of available reactivity, the doppler feedback almost balances the input reactivity, leading to long periods of almost steady power operation preceding the final non-energetic disassembly.

From this it appears that for disassembly to be complete, enough reactivity must be available to reach just beyond the final power peak, any further insertion being irrelevant. This brings

the reactivity required down to within a range from \$1.9 to \$3.0, with an average value of \$2.35. As more than this is available, the use of unrestricted ramps does not introduce a source of error. The insertion of about \$2.5 of reactivity could be brought about by: the inward movement of all subassemblies by 15mm; all the fuel being uniformly compacted vertically by 50mm; the fuel in 13 subassemblies melting and being compacted at the bottom of the core or roughly one quarter of the coolant being voided from the core (40). Although highly unlikely, such events are not completely physically impossible. The important factor is the rate at which this reactivity could be inserted by a plausible means. It is highly improbable that this could be more than a few hundred dollars a second.

6.4.2 Form of Reactivity Input

Although most of the parametric surveys in this field are carried out using uniform ramp rates of reactivity addition, this is by no means the only form of input possible, and physically is not even the most probable. It is used to such an extent due to its simplicity and convenience.

Rather than a uniform ramp, a reactivity input function of the type shown in Fig. 6.16 seems much more realistic. This figure actually shows the reactivity predicted by SAS for an EOEC LOF case for core B (37). The highly non linear nature of the curve is dictated by the voiding and slumping patterns during the end of the predisassembly phase.

Here, two forms of non-linear reactivity input will be discussed. The first of these is a piecewise-linear approximation to the type

of function shown, and the second is the input of varying amounts of reactivity as a step.

6.4.2.1 Piecewise Linear Reactivity Input

The results presented are from core A, for an initial ramp rate of 100s^{-1} , increased to 500s^{-1} at various points in time. In all cases the doppler coefficient was -0.005 . The resulting excursion yields are shown in Fig. 6.17 and Table 6.8. The reactivity, power and energy histories for some of these cases are shown in Fig. 6.18a, b, c respectively. As can be seen, the overall behaviour is rather interesting.

If the ramp rate is increased after the second power peak-region IV - very little difference to the yield is made, and the yield tends to the 100s^{-1} base case value. This agrees with the finding of Section 6.4.1 that reactivity inserted after the start of disassembly has little or no effect. If the ramp rate is raised in between the two peaks - region III - the effect is to increase the yield to even above the 500s^{-1} base case value. Raising the ramp rate just before the first peak-region II - brings the yield down below the 500s^{-1} value, but still well above the 100s^{-1} level. As the switch-over point becomes even earlier - region I - the yield tends to the 500s^{-1} base case value.

This behaviour may be explained with reference to the reactivity, power and energy histories shown in Figs. 6.18. In region I, the power and energy at the switch-over point are still low, and no feedbacks are yet significant. The result is basically the same as a 500s^{-1} ramp, but with slightly higher initial power, and hence

slightly higher final energy. In region II, the reactivity starts to rise at a time when the doppler feedback is beginning to become significant, and the rate of extraction of reactivity by the doppler effect is rising rapidly. Hence, the net reactivity and therefore power do not rise as rapidly or as high as before, reducing the final energy somewhat. In region III, the doppler reactivity is rising more slowly, and the rate of extraction is actually falling ($\frac{d^2f}{dt^2}$ - ve). The net reactivity and power have more opportunity to rise before the doppler feedback can overcome the extra reactivity, giving the observed high values of final energy. In region IV disassembly conditions are reached before the additional reactivity can have much effect, and the yield rapidly falls off to the $100s^{-1}$ base case value.

For lower values of doppler constant, where the interval between peaks is shorter or non-existent, region III will shrink, both in duration and in effect on yield. The opposite will hold true for higher doppler constants, where the time interval between power peaks becomes larger.

6.4.2.2 Step Reactivity Input

The results are again for core A, with varying amounts of reactivity being input as steps, over a range of doppler coefficients. The range of doppler coefficients is the same as in the standard result set, -0.003 to -0.0065. The reactivity input step sizes range from \$1.75 to \$6.0. The upper limit was set with regard to the amount of reactivity physically available for such rapid insertion.

Below \$1.75, it was found that the transients were so mild that no significant excursion yield was generated.

The resulting excursion yields are shown in Figs. 6.19a and 6.19b. As can be seen from Fig. 6.19a, for step sizes above \$3.0, the excursion yield once again becomes an exponential function of the doppler coefficient. This is not surprising, as we have the same conditions as in the case of high ramp rates: rapid insertion of large amounts of reactivity and very short transients. From Fig. 6.19b, it can be seen that for smaller step inputs, there is a region where the excursion yield is a linear function the reactivity step size.

It has already been shown (Section 6.4.1) that the final power peak/start of disassembly is the cut-off point for meaningful reactivity insertion, and the reactivity values at this point were given for several cases in Table 6.6. Comparing the excursion yields for these cases taken from Fig. 6.7a with the excursion yields for step inputs of the same size, taken from Fig. 6.19b, it appears that an amount of reactivity inserted as a step will result in a higher excursion yield than if it had been inserted as a ramp.

6.5 The Role of Fission Products

6.5.1 The Modelling of Fission Products through the Equation of State

As EXTRA assumes constant density throughout the course of the transient, the Equation of State (EOS) used is necessarily density independent. The tabulated energy-temperature and energy-

pressure relationships used are based on the EOS of UO_2 originally produced by Booth (41), as revised by Alderson. These are shown graphically in Figs. 6.20a and 6.20b. The vapour pressures are those of saturated UO_2 .

There is no vapour pressure generation until the fuel has reached its melting point, and as was shown in Fig. 6.13, the vapour pressure does not become significant until very late in the transient, when the local energy densities have reached high values - well over 1.5kJgm^{-1} .

It has been suggested (42, 43), that the gaseous and more volatile metallic fission products, such as Krypton, Xenon, Caesium and Tellurium which are formed once the fuel has been irradiated for even a while, can produce pressures of the order of well over 100bar at energies where the UO_2 vapour pressure is still insignificant. This would have a great effect on the course of the transient, as disassembly would be brought about earlier and at a lower energy, with a reduction of excursion yield.

To try and account for this effect, the energy-pressure relationship was changed to the form given in Fig. 6.20^c. Curve 1 represents the original vapour pressure of saturated UO_2 , whilst curves 2 and 3 represent two different cases considered in ref. (42).

Curve 2 - Brook '10' - is taken from Fig. 10 of ref (42). This is a case in which all or nearly all of the subassemblies in the core are initially in their normal operating state i.e. full power, full flow. The alkali metal groups near the surface of the fuel would quickly vapourise and together with the gaseous fission products would lead to a rapid increase in pressure. This would be enhanced by the small amount of voidage available for expansion. As it is assumed that no fuel motion takes place before fuel melting, this pressure

is introduced linearly over the fuel melting range and eventually smoothly joins the UO_2 vapour pressure curve.

Curve 3 - Brook '18' - is taken from Fig. 18 of ref. (42). This represents a meltdown accident, in which the power transient is preceeded by coolant voiding and some fuel and clad melting. The time scale of this is long enough to allow all the volatile alkali metals and released fission gases to have been swept out of the core. The only fission products left to provide pressurisation are those still retained in the fuel matrix. Because of the assumed high voidage, these have a large volume to expand in, further reducing the pressure generated. In both cases described the burnup is 5% and the core is assumed to be completely voided.

So far, thermal equilibrium between the fission products and the hot fuel has been assumed. This may not always be the case: for example fission gases may be escaping from the plenum as well as from the central cavity, in which case they would be at a slightly lower temperature, and would therefore have lower pressures than those calculated. This is allowed for in curve 4 by reducing the Brook '18' pressures by an arbitrary factor of 4.

6.5.2 The Effect of Fission Products on Excursion Yield

The fission product pressure representations just described influence the disassembly feedback as shown in Fig. 6.21. This strengthening of the disassembly term in turn affects the excursion yield as shown in Fig. 6.22. Ramp rates of 10s^{-1} , 50s^{-1} , 100s^{-1} and 500s^{-1} are considered, over the range of doppler coefficients -0.003 to -0.0065 .

At all ramp rates the presence of fission products reduces the excursion yield, though this is much more evident at low ramp rates than at high ones. With a 10s^{-1} ramp rate, the excursion yield was completely suppressed by all the fission products, while at 500s^{-1} the reduction is by no more than 30%. It is also obvious that the high pressure Brook '10' representation is much more effective at reducing the yield than the lower pressure representations.

At low ramp rates, the disassembly feedback is introduced progressively earlier and earlier, terminating the transient before there has been much energy deposition. At higher ramp rates, the transient is over so quickly, even without the added pressures of the fission products that there is little or no time for the fission products to have a mitigating effect.

The reactivity, power and energy histories for one case are given in Figs. 6.23a, b, c respectively. This is the 100s^{-1} ramp rate, doppler coefficient -0.005 case discussed earlier, but using the Brook '10' fission product representation. The disassembly feedback starts becoming significant some 2 msec. earlier than in the base case, bringing the net reactivity down earlier. The doppler reactivity is reduced because of the lower energies and hence temperatures reached. The stronger and earlier disassembly feedback more than compensates for this and the net reactivity falls off more quickly. This is reflected in the lower power peak and reduced final energy level. The corresponding traces using the other two fission products representations are bounded by the ones shown.

The way the presence of the fission products during the transient affects the final peak fuel temperatures and pressures is shown in Table 6.9. The pressures given here are the UO_2 vapour pressures corresponding to the final peak energy density, neglecting any fission

product contribution. The fission product pressures are also ignored in calculating the excursion yield, as this is deemed to be the work done by the expanding vapourised fuel. As can be seen, the reduction in UO_2 vapour pressure is quite dramatic, especially at lower ramp rates.

6.6 Restraints on Fuel Motion During Disassembly

6.6.1 Sources of Restraint

So far, the disassembly feedback term has been calculated with the assumption that the molten fuel is free to move both axially and radially under the influence of the fuel vapour pressure gradients. In cases where not only have the fuel pins melted, but the subassembly wrappers have also lost their mechanical strength, this is not too bad an assumption. However, in many other cases, such as the low fuel-vapour-pressure fission-product driven disassembly cases described in the previous section, this assumption is invalid. Although the fuel pins may well have melted, the wrappers will still be strong, forcing the molten fuel to move only in an axial direction.

In this section, two ways of modelling such restraint in EXTRA are described, together with their effect on the course of the transient and the resulting excursion yield.

6.6.2 Radial Restraint

This is a user option in the original version of EXTRA. If chosen, it causes all radial components of the disassembly feedback

term to be ignored. The disassembly feedback produced thus is compared to the unrestricted one in Fig. 6.24. As is to be expected, the removal of the radial component weakens the disassembly feedback.

The weaker disassembly term gives the somewhat higher excursion yields shown in Fig. 6.25. Here ramp rates of 10s^{-1} , 50s^{-1} , 100s^{-1} and 500s^{-1} are considered over the range of doppler constants $-0.003/-0.0065$, except for the 500s^{-1} case which only has a representative value at a doppler constant of -0.005 . At 10s^{-1} the increase in excursion yield is large, approximately 70%, but at the higher ramp rates this has fallen to less than 20%.

The reactivity, power and energy histories for the 100s^{-1} doppler coefficient -0.005 case are given in Figs. 6.26a, b, c respectively. The disassembly feedback comes in slightly later and more slowly than before. This allows the net reactivity and hence power to decay more slowly, giving slightly higher energy deposition. The doppler feedback is a few cents higher because of the higher energies and hence temperatures reached, but this is lost in the 50 or so cents difference in disassembly feedbacks.

In this particular case, the increase in yield is less than the average for this ramp rate. This is because at the time the disassembly feedback becomes significant, the net reactivity is already being brought down rapidly by the doppler feedback. The delay in disassembly thus has a small effect on the net reactivity and power level. In other cases, where the disassembly feedback comes in at a time when the net reactivity is falling more slowly, the delay has a larger influence.

Several of the cases described with fission product representation were re-run with radial restraint, resulting in the excursion yield

curves shown in Fig. 6.27. These are of the same form as before (C.F. Fig. 6.22), but all somewhat higher. However, although the fission products still reduce yields considerably, they are not as effective as in the unrestrained case. For example, with radial restraint, the Brook '18' fission products reduce the excursion yield of a 100s^{-1} ramp by 24%. Without radial restraint, the comparable reduction in yield is 37%. This is shown in more detail in Table 6.10. As before, the fission products are more effective at low ramp rates than at high.

6.6.3 Threshold Pressure For Fuel Motion

In this section an attempt is made to model the effect of the way the fuel could be prevented from moving until a threshold value of fuel vapour pressure is exceeded. Such a restriction on motion can be thought of as being due to a combination of the residual strengths of the unmolten fuel outer annulus and the fuel pin cladding.

An extra step was written into the calculation of the disassembly feedback - total energy table. For each total energy value in the table, the local pressure at every mesh point in the core is compared with a user input threshold pressure. If this pressure is exceeded, the calculation of the disassembly feedback proceeds normally, but if it is not exceeded, then the contribution to the disassembly feedback from that mesh point is ignored. The effect of this process on the disassembly feedback is shown for different values of threshold pressure in Fig. 6.28. The 'kink' in the feedbacks shown comes from the discontinuity in the internal energy-pressure relationship at the Critical Point. This becomes more apparent in the higher

threshold pressure cases as the contributions from the lower pressure region which would smooth this kink out, are ignored. At high energies, the feedback tends to the base case value as more and more mesh points exceed the threshold pressure.

The effect of varying the threshold pressure is shown for ramp rates of $5s^{-1}$, $10s^{-1}$, $20s^{-1}$, $50s^{-1}$ and $100s^{-1}$ in Figs. 6.29a-e respectively. Threshold pressures of 1, 10, 25, 50 and 100 bar are considered. A few cases with a threshold pressure of 1000 bar were also run. A comparison with Fig. 6.25 shows that up to $20s^{-1}$, a threshold pressure of only about 1 bar will raise yields by the same amount as radial restraint. By $100s^{-1}$, the threshold pressure required for this has increased to about 10 bar. Above 10 bar, and especially at ramp rates below $50s^{-1}$, the yield appears to lose most of its dependence on the doppler constant, and appears to be mainly a function of threshold pressure.

At low ramp rates, the input and doppler reactivities tend to balance each other out at a net value of reactivity just a few cents below prompt critical. This balance is then maintained until disassembly begins and terminates the transient. Here, this period of balance is prolonged until just enough energy has been deposited to allow disassembly to start. This is illustrated in Fig. 6.30 for a $10s^{-1}$ ramp with a threshold pressure of 50 bar. At these low ramp rates not much more energy can then be deposited before the transient has been terminated. As the disassembly feedback with threshold pressure limitation has a much sharper energy 'cut-off' value than before (Fig. 6.28), the effect is to make the yield much more dependent on the energy at which the feedback becomes significant, and hence on the threshold pressure.

This trend is also visible at higher ramp rates, but the doppler retains more of its previous influence. In these cases, where the energies and temperatures are changing more rapidly, there is no period of steady net reactivity. The yield will depend on how many extra power peaks have been allowed to occur before disassembly and on the exact relative timing of the final power peak and disassembly.

The increase in yield, especially at low ramp rates and high threshold pressures, is very large. To some extent, this is because contributions to the yield are calculated from all meshes, regardless of whether they are above the threshold pressure or not. Fig. 6.31 shows the effect of assuming that meshes still below the threshold pressure at the end of the transient have not failed and therefore do not contribute to the excursion yield. As can be seen from the figure and from Table 6.11, the result is quite dramatic, especially at high threshold pressures, where only a small proportion of the core is deemed to have 'failed' (C.F. Fig. 6.14). For example, whereas at 10s^{-1} a threshold pressure of 50 bar increases the yield by 1382%, if the yield is only taken from those meshes above 50 bar the increase in yield is only 404%. Although this is still a large increase, it is much smaller than before. At ramp rates of 100s^{-1} and above, the yield may actually be reduced by medium threshold pressures if the yield is only calculated from those meshes above the threshold pressure. In these cases although the thermal yield and hence final pressures have increased from the base case, the threshold pressure has risen more. This reduces the number of meshes contributing and thus reduces the yield. At lower ramp rates the situation is reversed.

Table 6.11 also shows the effect of radial restraint on cases with the threshold pressure model. The yields are again increased,

but not quite so much as in the comparable unrestrained case. This is similar to the way mechanically restrained fission products are not quite as effective in reducing yield as when unrestrained (Table 6.10).

Fig. 6.32 shows the effect of a combination of the Brook '10' fission products described in section 6.5.1 and different threshold pressures on a ramp rate of 100s^{-1} . As the threshold pressure rises, the yields gradually tend to the base case. For high enough threshold pressures, or with lower pressure fission products, the yields would certainly be increased above this level. Combinations of threshold pressure and fission gas pressure will exist for which the opposing effects will cancel out, leaving little visible influence on the excursion yield. Nevertheless, comparison with Fig. 6.29e shows that the fission products are effective in reducing the yield from a comparable case with no fission products.

6.7 Initial Conditions

6.7.1 Introduction

The main parameters defining the initial core conditions in an EXTRA run are the initial power, reactivity and average core temperature. In all the cases considered so far, these have been chosen to represent the normal steady state operating conditions i.e. full power, zero excess reactivity, average core temperature 1500K and neutron precursors in balance with the initial power level.

However, when EXTRA is linked to a predisassembly code, the core conditions at the start of the EXTRA run are very different:

power level up to 200 times normal, reactivity near prompt critical, much of the core near melting if not already molten and neutron precursor densities out of balance with the power level. In this section, the effects of such changes in initial conditions will be investigated. Although all these parameters are interrelated and are not truly independent of each other, they will be treated as if they are for ease of presentation. The parameter survey reported applies throughout to core B. From a few check calculations it appears that core A behaves in a qualitatively similar fashion, allowing for the slight differences noted in Section 6.3.

6.7.2 Initial Temperature

The initial temperature in EXTRA is defined as the temperature at the energy zero of the equation of state (EOS) energy-temperature table, previously shown in Fig. 6.20a. As the energy at the start of a transient is always zero, this gives an initially flat temperature profile, which then gradually takes on the shape of the power distribution as energy rises. This may be acceptable when EXTRA is used in a 'stand-alone' mode, but is not very adequate when the initial conditions are those at the switch over point from a pre-disassembly calculation. Newer versions of EXTRA allow for a non-uniform initial temperature and non-zero initial energy distributions. This refinement has not been programmed into the version of EXTRA available at the College due to lack of time and difficulties with mocking up self consistent and meaningful initial energy distributions.

Variation in initial temperature were treated by redefining the energy zero of the EOS tables. The energy at the desired new

initial temperature was set to zero, and the original energy at this temperature was subtracted from all subsequent entries in the table. All the energies in the energy-pressure table were reduced by the same amount to preserve the pressure-temperature relationship. Fig. 6.33 shows how this affects the build up of average temperature with total energy in core B for initial temperatures of 1900K, 2300K and 3000K. The large 'flat spot' on the 3000K curve is caused by the addition of the latent heat as fuel melts, and is enhanced by the initial flatness of the temperature profile. At such a high initial average temperature, it is to be expected that a fair proportion of the fuel in the core is already molten. To roughly account for this, the latent heat was greatly reduced with the effect of smoothing out the build up of temperature. The build up of doppler feedback and disassembly feedback with total energy is shown in Figs. 6.34a and 6.34b respectively where a similar smoothing effect can be seen. The resulting excursion yields for ramp rates of 10s^{-1} , 50s^{-1} , 100s^{-1} and 500s^{-1} over a range of doppler coefficients are shown in Figs. 6.35a-d respectively.

At low (10s^{-1}) ramp rates, the yield is reduced for all values of doppler coefficient. This is because although initially the net reactivity and power rise a bit faster due to the reduced doppler feedback, disassembly occurs very much earlier and consequently much less energy can be deposited.

At high (500s^{-1}) ramp rates, the yield is increased for all values of doppler coefficient. Here, power rises so quickly that in spite of earlier disassembly, the reduced doppler feedback allows more energy to be deposited.

The two intermediate ramp rates lie in a change-over zone between these regimes. For example, at 100s^{-1} and low doppler

coefficients, the yield is increased, for the same reason as above. At higher doppler coefficients, the yield is reduced. In this region, the yield is governed by the doppler influenced multiple power peaks. The weakening of the doppler feedback combined with earlier disassembly reduce and finally remove the second power peak thus reducing the energy deposited and hence the excursion yield. At 50s^{-1} , the yield reduction seen at low ramp rates is beginning to be nullified by the faster reactivity insertion and associated faster power increase before the onset of disassembly

6.7.3 Initial Power

The survey of the effects of different initial power levels has a somewhat different format to the preceeding work, being limited to one ramp rate, 50s^{-1} , and one doppler coefficient, -0.00378 . Table 6.12 shows how the peak relative power, peak net reactivity, final average fuel temperature and thermal yield vary when the initial power is increased to 50, 100 and 200 times the nominal value. Initial temperatures of 1500K and 3000K are considered. The initial reactivity is zero, and the delayed neutron precursors are in equilibrium with the initial power. The relevant power histories are shown in Figs. 6.36a, b.

As can be seen from Table 6.12, as initial power rises, all the other parameters shown fall drastically for both initial temperatures. The higher power levels seen allow temperatures and pressures to rise much more quickly than before. This in turn causes the doppler and disassembly feedbacks to become significant much earlier giving lower

peak net reactivities and lower thermal yields. There are two competing effects here, the indirect one of stronger feedbacks reducing peak power and yield, and the direct one of higher initial power giving higher peak power and yield. At an initial temperature of 1500K, the doppler feedback is strong enough to suppress the direct effect completely, but at 3000K the doppler is sufficiently weakened to allow the direct effect to be seen in the slight increase in peak power and thermal yield for an initial power of $200P_0$.

6.7.4 Initial Reactivity

Here, the initial power was held at its nominal value of 975M.W., whilst the initial reactivity was increased to \$0.95, \$1.00, and \$1.05. This was done by suitably adjusting the neutron source term in the kinetics equation. This is normally taken to be zero. As before, the delayed neutron precursors were in equilibrium with the initial power and the same initial temperatures, 1500K and 3000K, were used. Table 6.13 shows the resulting changes in peak relative power, peak net reactivity, final average fuel temperature and thermal yield. Relevant power histories are shown in Figs. 6.37a, b.

For both temperatures, the three reactivities near prompt critical all give slightly higher yields compared to the zero initial reactivity case. However, amongst themselves they vary by less than 6%. The power histories are also very similar, all showing how the higher initial reactivity causes power to rise more quickly. As it is generally thought that the net reactivity predicted at the end of a predisassembly run should not be more than a few cents out (the

net reactivity at crossover to disassembly, as predicted in the code comparison reported in Chapter 4, agreed to within 2 cents) it is comforting to note that small variations in net reactivity around prompt critical have only a very small influence on the yield.

6.7.5 Initial Delayed Neutron Precursor Density

In the standard version of EXTRA, the delayed neutron precursors are always assumed to be initially in equilibrium with the initial power, and this is what has been done in all the cases discussed so far. This is adequate if the transient starts when the reactor is operating at a steady state e.g. full power. However, at the crossover point from a predisassembly code such a balance between power and delayed neutron precursors will not exist, the actual precursor levels depending on the detailed course of the transient up to that time. This has been catered for in the version of EXTRA which accepts data for core B, by a user option to read in any desired alternative values of initial delayed neutron precursor density.

In general, it was found that the thermal yield is most sensitive to initial precursor density, for all values of initial power, when the initial reactivity is zero. If the initial reactivity is near prompt critical, changing the initial precursor densities by several orders of magnitude will only change the yield and other associated parameters by at most a few percent. An example of this is given in Table 6.14. This gives the changes in peak relative power, peak net reactivity, final average fuel temperature and thermal yield when the precursor densities are reduced by several orders of magnitude

from their equilibrium values. As can be seen, the differences are very small. The implication is that precursor densities do not have to be calculated with extreme accuracy.

Fig. 6.38 compares the excursion yields obtained for ramp rates of 10s^{-1} , 50s^{-1} , 100s^{-1} and 500s^{-1} over a range of doppler coefficients for two cases. The first is the standard case with initial power P_0 , zero initial reactivity and delayed neutron precursors in balance with the initial power. In the second case, the initial power is $50P_0$, initial reactivity is 0.95 and the delayed neutron precursor densities are the cross-over values predicted by Meyer et al (37). As can be seen from Table 6.15, they are somewhat higher than the equilibrium values as a result of their build up during pre-disassembly. Qualitatively the two cases show very similar behaviour. The main differences are almost certainly due to the competing effects of higher initial power in lowering the yield and higher initial reactivity in raising the yield. At 50s^{-1} and 100s^{-1} the oscillations in the yield curve caused by the doppler effect appear to be smoothed out.

6.8 Comparison with Published VENUS Parameter Surveys

In this section, the results of the author's parameter surveys for EXTRA will be compared with similar parameter sensitivity studies carried out using the VENUS code. The first comparisons are with the studies carried out by Meyer et al (37). The initial conditions for their VENUS-II runs were obtained from a SAS3A predisassembly calculation for a LOF case. The initial conditions in the EXTRA runs

attempt to follow these as closely as possible. As in this case the core model used in EXTRA is based on the one used in VENUS-II, the results should reflect basic differences in code predictions without being too much influenced by other factors.

Table 6.16 shows a comparison of initial power and reactivity parametrics. Although EXTRA predicts consistently higher values, e.g. thermal yield is about 16% higher, the behaviour is qualitatively the same. The power and reactivity histories for the base case: initial power $50P_0$, initial reactivity $\$0.95$, ramp rate $\$50s^{-1}$, doppler coefficient -0.00378 , are shown for the two codes in Figs. 6.39a,b,c. The higher peak power and hence higher final energy level in EXTRA is caused by the peak net reactivity being a few cents higher. This in turn is due to the doppler feedback being slightly weaker at first. Eventually the faster rise in power in EXTRA causes a rapid increase in doppler feedback bringing the net reactivity, and hence power, down faster than in VENUS. The total amount of reactivity extracted by the doppler effect is the same in both cases. Although the final rate of reactivity extraction by disassembly is roughly the same in both codes, VENUS seems to build up to this slightly more gradually than EXTRA. This is probably a reflection of the differences in the methods of calculating disassembly feedback discussed in Chapter 5. Overall, the general behaviour is very similar. The timing of events, such as the power peak and the onset of disassembly, agrees to within lms.

Table 6.17 shows a comparison of ramp rate parametrics between the two codes. As before, although EXTRA gives consistently higher results, the overall behaviour pattern is the same.

Finally, several key parameters were varied about their base case values, and the relative effect on the thermal yield observed. Table 6.18 compares the results of this with values taken from the studies of Meyer et al (37) and Nicholson and Jackson (44). This last is a detailed parameter sensitivity study of the Fast Flux Test Facility (FFTF) using the VENUS-I code. The initial conditions for the VENUS runs were taken from a MELT-II analysis of a TOP transient. Obviously, as FFTF has a much smaller core, the absolute values of the various parameters are quite different from those considered up to now, but this does not matter as we are only interested in relative changes. As can be seen from Table 6.1 the codes produce remarkably similar relative changes in thermal yield for the same relative parameter change.

On the basis of this very limited exercise, it appears that compared to VENUS, EXTRA overestimates thermal yields by around 16%, net reactivity by 2-3 cents, final temperature by about 150K, but in general behaves in a very similar fashion.

6.9 Final Conditions

In this section, the assumptions about conditions at the end of the expansion will be discussed. To recap Chapter 5, at the end of the transient, when the power has fallen to some fraction of its initial value, the core is assumed to still be in its original configuration, but with very high levels of pressure and temperature. These pressures and temperatures correspond to the distribution of the energy deposited in the core. This energy is the thermal yield and is the integral of the power time-history over the transient. This

rather unrealistic representation is forced on the code by its use of virtual motion and constant density in the calculation of disassembly feedback. Core displacements are never explicitly calculated hence there is no pressure relief during the transient due to material motion. This may well explain the faster build-up of disassembly reactivity compared to VENUS, which does allow for pressure relief.

Once the transient is over, the core is allowed to expand adiabatically until some chosen uniform final pressure is reached. The excursion yield, i.e. the energy available to do mechanical work on the surroundings, is just the difference in energy before and after expansion. This is also a rather unrealistic picture as in fact the fuel vapour bubble can only expand until all the free volume in the containment vessel has been taken up. If this occurs before the desired final pressure has been reached, the strength and response of the containment vessel will be important. Even this is very simplified, as ideally the compression of the cover gas by the expanding fuel vapour should also be considered.

The response of the containment vessel to such an impact is a very complex topic which is the subject of a whole class of codes, known as containment response codes, and is outside the scope of this study. Nevertheless, it is interesting to estimate under what circumstances the available free space could be taken up.

So far, the final pressure at the end of the expansion has always been assumed to be one bar. The effect of a strong containment would be to terminate the expansion of the vapour at a higher final pressure. Figs. 6.40a, b show for cores A and B respectively the effect on excursion yield of increasing the final pressure to 2, 5, 10, 20, 50 and 100 bar. As can be seen, for any thermal yield, increasing the final pressure reduces the excursion yield, the effect falling off with in-

creasing thermal yield. The corresponding vapour fractions (fraction of active core vapourised) at the end of the expansion are shown in Figs. 6.41a, b. Again, at high thermal yields these tend to a common value for all pressures. This is to be expected, as at higher thermal yields larger proportions of the core will initially be above the final pressure and hence will be able to expand and contribute excursion yield. As has already been noted in Section 6.3, core B has higher excursion yields than core A for the same thermal yield.

The volumes occupied by the two cores at the end of the expansion are shown in Figs. 6.42a, b. The vapour fraction distribution is used together with UO_2 vapour and liquid specific volumes corresponding to the chosen final pressure to obtain the final specific volume of each mesh cell. From these and the constant masses of each cell the final volume of the core is calculated. Meshes with pressure initially below the final pressure retain their original volume. Hence, at high final pressures and low thermal yields there is no expansion at all and the core stays at its normal size. In such cases there is of course no excursion yield. Such a combination might represent a case in which sufficient fuel pins have failed to allow the transient to be terminated by small fuel displacements, but the wrappers and other core containment structures have retained enough strength to prevent any significant vapour expansion. This could well lead to the transition phase. At higher thermal yields and with lower final pressures, the final volume increases with thermal yield. In these cases the wrappers must be assumed to have lost all their strength.

Redrawing Figs. 6.40, 6.41 and 6.42 with final pressure as the dependent variable instead of thermal yield allows Figs. 6.43 and

6.44 to be drawn. These results were obtained using the YIELD option in EXTRA. The thermal yields input were chosen to give excursion yields of 10, 20, 50, 100, 200, 500 and 1000M.J. on expansion to one bar, and this explains the seemingly strange progression of thermal yields shown. Figs. 6.43a, b show how the excursion yield varies with final pressure along lines of constant thermal yield and constant final volume. Figs. 6.44a, b do the same for the vapour fractions. From these two figures, knowing any three out of: the thermal yield, excursion yield, final pressure, final volume, or vapour fraction, the missing parameters can be defined. Allowing for the already noted differences between the cores, the two plots are basically the same.

From these figures it also becomes clear that the final expansion is one area where pool and loop reactors could perhaps be treated differently, even in an EXTRA calculation which apparently ignores the containment. Core A, being from a pool reactor, has an associated cover gas volume in the region 500-1000m³. Core B, being from a loop reactor has a cover gas volume of only about 21m³. This is because the pool reactor containment has to accommodate several coolant pumps and heat exchangers apart from the core, whilst a loop reactor containment vessel is only big enough to take the core itself. The implication is that the containment vessel of core B will have to withstand much higher pressures than that of core A.

CHAPTER 7

CONCLUSIONS AND RECOMMENDATIONS

7.1 Conclusions

In Chapter 6, the results of different modelling assumptions during the disassembly phase were presented, and the effects of various implications noted. An example of this is the potential of the retained fission gasses to build up sufficient pressure to terminate the transient at lower energy levels.

However, taking a view of the results presented as a whole, the following can be observed. At ramp rates of 50s^{-1} and above, and providing that reasonable modelling assumptions are made, it is hard to change the excursion yield by more than a factor of two, other than by changing the reactivity input function - Section 6.4.2. In other words, the excursion yield appears to be relatively insensitive to modelling assumptions. Examples of 'unreasonable' assumptions which can change the yield by more than a factor of two include the Brook '10" fission product representation, which almost certainly overestimates the fission gas pressures, and threshold pressures for fuel motion in the region of 1000 bar, which overestimate the strength of the fuel and cladding. At lower ramp rates the effects of modelling assumptions are more marked, but the region of 50s^{-1} - 200s^{-1} is of especial interest as this seems to be the order of ramp rate predicted by predisassembly codes at cross-over to disassembly and is also within the area of apparent yield insensitivity.

The insensitivity of yield to modelling assumptions is borne out by the comparison with VENUS-II. As was shown in Chapter 5, VENUS-II

has completely different methods of calculating disassembly feedback, yet the predicted yields differ by no more than 15% from those calculated by EXTRA. In fact, the difference seems largely due to different power levels during the doppler controlled part of the transient. It is possible that once the temperature initialisation in EXTRA is improved (Section 6.7.2) the discrepancy between the two codes will be reduced still further. Additional evidence for the relative unimportance of modelling assumptions during the disassembly phase can be seen in Fig. 7.1. Here, the power traces calculated by VENUS-II (using point kinetics) and FX2POOL (using space-time kinetics) for a 50s^{-1} ramp rate in a 1000 mw(e) core can be seen to be virtually the same.

Although the range for comparison was limited to those VENUS-II cases appearing in the open literature, it does appear that under many circumstances there is little or no advantage to be gained from using a detailed but slow running and therefore expensive code such as VENUS-II, rather than a simpler but faster and cheaper code such as EXTRA. If the actual core displacements and an accurate estimate of the pressure pulse at the end of the transient are required, say for use as input to a containment response code, then obviously a VENUS-type code would have to be used. However, if all that is required is the variation of excursion and thermal yield over a range of parameters, a code such as EXTRA seems quite adequate, especially when the uncertainties in the material properties data are considered.

To sum up, the way and rapidity with which reactivity is inserted seems to be the main controlling factor in determining the excursion yield, as in general the calculation appears relatively insensitive to other assumptions. The other controlling parameter, the doppler coefficient, can usually be estimated fairly accurately.

This emphasizes the need for modelling accuracy and detail in the predisassembly phase. The precision with which the reactivity input function to the disassembly phase is defined will determine whether the yield produced is acceptable or not in terms of containment response.

This in turn means that the models used during the predisassembly phase should be as detailed and as accurate as possible. Chapter 4 indicated that, on the whole, agreement between various codes is reasonable, but the difference in driving ramp rates by a factor of two implies that much more work remains to be done in this field. A change in ramp rate by a factor of two can change the yield by a factor of four. This is more than the change produced by, say, radial restraint or Brook '18' fission products.

In the predisassembly phase, one of the major uncertainties is in the role of the FCI. If this can take place with anywhere near full efficiency, the resulting voiding ramp rates and hence yields will be very high. However if, as is thought by Fauske (e.g. 46) and others (e.g. 47), an energetic FCI is highly unlikely if not impossible, the ramp transferred to the disassembly phase will be much smaller. In such a case, the transient may well end up with a non-energetic benign shut-down. Even with fairly mild FCIs, in most cases unrealistic assumptions such as core mid-plane pin failure have to be made in order to create the necessary conditions for disassembly. As all predisassembly codes are in practice limited to a small number of channels when compared to the number of sub-assemblies, they all display much more coherent behaviour than would be expected in real life. Increased incoherence would tend to have a mitigating effect by lengthening the duration of the transient and hence reducing the ramp rates produced.

Another uncertainty is the degree of inaccuracy introduced by the use of point kinetics and lumped reactivity parameters obtained from first order perturbation theory. Although the latest version of SAS, SAS4A, is supposed to include full space-time kinetics, as yet no results appear to have been published, so the importance of this is difficult to estimate. In some circumstances it could well be significant. This would certainly be the case if the core were heading for the transition phase and considerable fuel melting and relocation were taking place during predisassembly.

In general it can be concluded that in order to produce unacceptably high excursion yields, high ramp rates have to be predicted by the predisassembly phase. To achieve this, unrealistic and very pessimistic assumptions have to be made about the location and mode of fuel pin failure, and about the likelihood of an energetic FCI. If physically more reasonable assumptions are made, all but the most severe initiating circumstances appear to lead to benign shut-down or the transition phase with little or no energy release.

Above all, it must be borne in mind that the prime assumption in all cases has been the total failure of all reactor trip systems. This includes manual shut-down by the presumably well trained and alert operating staff. The list of pessimistic assumptions that have to be made before enough energy is released to breach the containment vessel is so long as to be deemed 'incredible'.

7.4 Recommendations

As the disassembly process seems relatively insensitive to modelling assumptions, priority should be given to improving predisassembly representation. Some work could be done to clarify the reason for this insensitivity. One reason is that the speed of the transient allows very little time for differences in disassembly feedback to become apparent, whilst the early part of the transient is doppler controlled. However, should work on more accurate predisassembly codes indicate that the ramp rates in disassembly are actually much lower than considered at present, disassembly codes will once again have to come under closer scrutiny.

In predisassembly codes, the main areas which still need further clarification include the likelihood and consequences of energetic FCI, and the accurate and reliable prediction of fuel pin failure. Pin failure at the core mid-plane easily results in core voiding and substantial voiding ramp rates, whereas the more likely failure location at the top of the core usually has negligible results.

The present trend of fast reactor analysis seems to be away from the highly energetic Bethe-Tait type of accident and toward a much milder non-energetic transition phase ending to the incident. This has resulted from a better understanding and better representation of the physical processes occurring. This has introduced the need for ever more complex and sophisticated codes to model the behaviour of the core during the transition phase, with its slight risk of secondary recriticality. The transition phase, and the way in which predisassembly codes can lead to this phase, forms a large area for future research. Such work would almost definitely involve the use of space-time neutron kinetics in the transition phase, and possibly in the predisassembly phase preceeding it.

APPENDIX I

1.1 MELT-III

1.1.1 General Introduction

MELT-III is a recent version of the MELT code series, developed at the Hanford Engineering Development Laboratory. The most recent version is MELT-IIIA, but this has many of the same features as MELT-III. The following description, though of MELT-III, is applicable to both codes, with the exceptions noted (48, 34).

The code is primarily used for TOP analysis. Eventually it should have a limited LOF capability, once the sodium boiling module is fully implemented in MELT-IIIA.

1.1.2 Core Representation

The core is split up into a number of flow channels, each channel being represented by a single fuel pin. Grouping of subassemblies into channels can be such that each channel contains subassemblies with similar burnup and power level, regardless of their geometrical position in the core. This lack of geometrical restraint means that 3-D neutronics data can be used directly. A typical representation is shown in Fig. A1.

MELT-III allows up to 20 channels, MELT-IIIA up to 100. The code is written such that data for only one channel at a time is in the computer memory. The only inherent limit on the number of channels is the extent of available peripheral storage.

Each pin representing a channel can have up to 10 axial zones, with a total of up to 75 axial nodes. A zone is defined as a region

where the flow area remains constant. One of these zones is the active core, which can have up to 20 axial nodes. The remainder are used to represent: the inlet orifice block, the lower and upper reflectors, the fission gas plenum, the handling socket (free flow area), the instrument tree and the sodium pool.

In the core zone, the pin has a fuel region, including central void (up to 12 radial nodes), a fuel/clad gap assumed gas filled, a clad region (up to 3 nodes), a coolant node and a structural node. This last is physically attached to the coolant node and is used to simulate a portion of the duct wall or wire wrap. The fuel pin representation is shown in Fig. A2.

1.1.3 Cladding and Fuel Behaviour

Originally, MELT-III provided three simple ways of predicting clad failure. The temperature could be checked at either a specified node or at all axial nodes against a set failure temperature. Once this temperature was exceeded, failure occurred at a specified axial code in that channel. The actual fuel enthalpy could be checked against a calculated clad temperature and power level dependent failure enthalpy. Failure occurred at the time and place this was exceeded, provided that molten fuel existed at that axial level. The simplest failure criterion input a time and location of failure for each channel. This was mainly useful in recreating experimental results, where time and position of failure were known.

A more recent criterion used in MELT-III and MELT-IIIA is the damage parameter correlation. This is based on the Larson-Miller

Parameter approach, and includes a transient rate effect, provides a smooth transition between micro structure types and reflects the rate effect of the cladding stress-strain behaviour. The Damage Parameter (D.P.) is proportional to the following factors:

$$D.P. = \frac{\left[\begin{array}{c} \text{Enthalpy} \\ \text{upset} \end{array} \right] \left[\begin{array}{c} \text{Enthalpy upset} \\ \text{decay term} \end{array} \right] \left[\begin{array}{c} \text{Retained} \\ \text{fission gas} \end{array} \right] \left[\begin{array}{c} \text{1-area of} \\ \text{columnar} \\ \text{zone} \end{array} \right] \left[\begin{array}{c} \text{sq. root of} \\ \text{time into} \\ \text{transient} \end{array} \right]}{\text{(Residual cladding strength as function of temperature)}}$$

This is defined to give a common value at failure, and is correlated to experimental results. All axial nodes are checked, and if the damage parameter exceeds the failure value at a point where fuel is molten, the clad is assumed to fail at that point,

Before the start of the transient, the materials behaviour code SIEX is used as a subroutine to calculate initial values of fuel-clad gap heat transfer coefficient, plenum pressure, plenum volume, plenum gas density, fuel fission gas retention, fuel porosity and to initialise the steady state temperature and fuel restructuring calculations. Before clad failure, the restructuring, fission gas release, thermal expansion and melting of the fuel are calculated together with their effect on fuel and gap thermal conductivity and on internal pin pressure.

When the clad fails, a mixture of fission gas and molten fuel is squirted into the coolant stream, starting an FCI. The driving pressure for the squirting is the pressure difference between the inside of the fuel pin and the coolant channel. Inside the pin, the non-uniform flow of the molten fuel/fission gas mixture in the internal cavity and through the rupture is followed using a modification of the code HOTPIM. This is a stand-alone code modelling the motion of molten fuel inside a fuel pin which has been incorporated into MELT as a subroutine. It has considerable modelling flexibility and solves the governing conser-

vation equations (energy, momentum and continuity) using the method of characteristics. Once fuel has entered the coolant channel, a certain fraction of it is assumed to plate out onto the cladding. This then affects the coolant flow area and reduces the amount of fuel taking part in the FCI. The fuel not plated out remains in suspension and is swept out of the core.

All thermal conductivities, except that of coolant vapour which is a constant, are considered temperature dependent. They are either supplied as tabular input or calculated from empirical correlations.

1.1.4 Coolant Behaviour - Voiding and FCI

Up to three independent primary coolant loops can be specified. Those channels connected to a common plenum volume are hydraulically coupled during the transient. Coolant flow through the primary loops is governed by a specified pump head vs time. Friction and inertial considerations are included in the hydraulic treatment, but heat transfer effects are ignored. The model is shown schematically in Fig. A3.

The individual channel hydraulic characteristics are specified by the areas of the various zones and the pressure drops across these zones. The pressure at area changes is calculated using the same formula for contractions and expansions, which is wrong by a factor of two. The inlet and outlet pressures for each channel are known from the primary loop calculations. The local pressures are obtained by iterating the mass flow rate and calculated pressure drop across each axial

zone to converge on the known outlet pressure, taking into account coolant density changes due to heating, and frictional forces.

As at the moment there is no coolant boiling model other than flash evaporation during the FCI process, only one coolant voiding mechanism is available. This is the release of a mixture of molten fuel and fission gas into the coolant channel through failed cladding and subsequent FCI.

Once the mixture has entered the coolant channel, a bubble is formed occupying the whole flow area. The bubble is assumed to behave like a massless frictionless gas for momentum balance purposes and like a homogeneous mixture for energy balance purposes. The internal pressure is uniform throughout the bubble and its expansion is determined from heat transfer and thermodynamic principles. The bubble is assumed to be in mechanical equilibrium with the surrounding liquid and to be in the saturated state. It defines two liquid slugs, initially driven apart. The slug velocities are defined using an inertial model. The primary driving force for slug ejection and bubble growth is evaporation of liquid coolant.

Molten fuel is squirted into the coolant channel for as long as a pressure difference exists between the pin interior and the coolant channel. Heat transfer from newly squirted fuel is not computed until after a certain delay, which models the reaction rate. The volume of the FCI zone is the sum of the volumes of the fission gas, vapour and liquid coolant, and molten fuel, adjusted for pressure. Heat transfer also takes place from coolant vapour to cladding and structural heat sinks.

1.1.5 Neutron Kinetics and Reactivity Feedbacks

The standard point kinetics formulation with up to six groups of delayed neutrons is used. The equations are solved using a variable time step march-out algorithm. At low reactivities this is replaced by a zero neutron lifetime approximation, which allows much larger time steps to be used.

The reactivity is the sum of the programmed reactivity and various feedbacks. The input reactivity may be in the form of a step, ramp or any other desired function including negative values to model a reactor scram. Reactivity feedbacks are taken from the doppler effect, coolant density change and voiding, fuel axial expansion and contraction, core radial expansion, and molten fuel motion inside the fuel pin and in the coolant channel.

The doppler feedback is a spatially weighted function of the fuel temperature change since the start of the transient, with allowance for sodium voiding and density change.

The coolant density change and voiding feedback is calculated from tables of coolant worths.

The fuel axial expansion feedback is calculated from changes in fuel mass in each node due to thermal expansion (or contraction) and tables of fuel worth. The reactivity from core radial expansion is obtained by multiplying a weighted average channel coolant temperature change by a constant.

The reactivity effect of fuel motion in the coolant channel including both fuel plate-out and sweep-out is obtained from fuel worth tables. The loss of fuel from inside the pin is also allowed for.

The material worths used are obtained by running data tapes from the 3DB-3DP neutronics code developed at HEDL through a special processing code. This gives fuel, coolant and cladding worths, power profile and Doppler weightings for the desired number, and sub-assembly composition, of channels.

1.2 SAS3A/SAS3D

1.2.1 General Introduction

The SAS accident analysis code system has been under development at the Argonne National Laboratory for about ten years. SAS3A is one of the most commonly used codes, at least in the States, and has been widely applied to the analysis of the American Fast Flux Test Facility (FFTF) and the Clinch River Breeder Reactor (CRBR). SAS3D retains the same basic models but has an improved data handling scheme and has been generally 'cleaned up'. This has resulted in an increase in the number of channels modelled, and a reduction of running time by a factor of nearly two. Unless stated otherwise, the following description applies to both SAS3A and SAS3D (49,50).

The code was primarily developed for unprotected LOF analysis, but it has the capability of treating TOP cases as well. There is a facility for providing output to a disassembly phase, usually the disassembly code VENUS-II.

1.2.2 Core Representation

The core is again split up into a number of channels, each containing a representative fuel pin. The subassemblies may be grouped into channels according to power/flow ratio and burnup, by subassembly row, or in any other convenient fashion. Of the two methods suggested, the first is superior, as it allows for a more accurate representation of incoherent effects. SAS3A is limited to ten channels. In SAS3D, only the data relevant to one channel is in the computer central memory at one time, shifting the limit on channels to the amount of peripheral storage available. SAS3D is written to accommodate up to thirty-four channels.

Two axial flow zones are allowed, though only one is usually used. Up to thirty axial nodes can be specified, to model the entire channel from inlet to outlet plenum. Radially, the channel is divided into fuel, fuel-clad gap, cladding, coolant and structural nodes, with provision for a central void region.

1.2.3 Cladding and Fuel Behaviour

Prior to failure, the cladding is allowed to melt and relocate. In the Lagrangian model used, discrete clad segments move under the influence of gravity, the channel pressure gradient, the frictional drag due to streaming coolant vapour and friction between the moving clad and the fuel pin.

Feedback effects are included to modify: the friction between coolant vapour and the channel, the coolant channel hydraulic diameter, and the coolant vapour flow area. Heat transfer is calculated

from moving clad to stationary clad and from stationary fuel to moving clad. The viscosity of the moving clad is a function of internal energy.

The model allows upward movement of clad under the high friction drag of flowing coolant. The molten clad may then refreeze in the upper channel above the core causing partial or complete blockage. The clad flow may then reverse to drain under gravity and block the lower end of the channel.

The fuel pin mechanics model assumes that fuel and cladding behave in a perfectly elastic and elastic-plastic fashion respectively. The resulting pin dimensions are used by a superimposed calculation which releases fission gas from the fuel to the central cavity. The fission gas pressures then load the cladding through the cracked strengthless fuel, producing pin failure when the cladding burst strength is exceeded.

If the clad fails in a non-voided region, fission gas release or FCI take place. If the channel is voided, and fuel melting has reached the unrestructured fuel zone, fuel collapse is assumed. In the failed region there is a compressible slumped zone which can move up and down the channel. Fuel motion in this area is calculated by a compressible hydrodynamics calculation, taking into account the influence of gravity, coolant vapour, fission gas, friction, and steel and fuel vapour driving forces. As fuel melts, individual axial fuel segments join the slumped region from above and below. Unmelted fuel above can fall into or be pushed out of the slumped region. The unmelted pin below is stationary.

Before the transient starts, a steady state portion of the code initialises coolant flow, fuel pin heat transfer characteristics,

detailed fuel microstructure and fission gas release. The assumption is made that the fuel has been irradiated throughout its lifetime at the same power level as exists at accident initiation.

1.2.4 Coolant Behaviour - Voiding and FCI

The primary loop model provides for a single coolant loop outside the core, connected to the inlet and outlet plenums of all the coolant channels and for a single by-pass channel - control rod and radial reflector assemblies for example. The outer primary loop contains representations of a pump, intermediate heat exchanger and piping. Primary loop flow is specified by pump head vs. time or flow rate vs. time.

The primary loop model supplies inlet and outlet plenum pressures to the coolant dynamics module, which returns detailed channel mass flow rates as input for the primary loop calculations. The channels are hydraulically linked. Their hydraulic characteristics are specified in terms of area and hydraulic diameter for the core region, upper and lower reflectors and plenums.

Coolant voiding is initiated by one of three means: coolant boiling, FCI or fission gas release. Boiling can start at any point in the channel where the coolant temperature exceeds a local time-dependent saturation temperature by more than a superheat criterion.

The boiling model allows the formation and continuing existence of a number of bubbles in a channel and follows them either as they move up the channel and collapse (or reach the exit) or as they become sustained and void the channel. Small bubbles are treated

with a uniform pressure slug ejection model. Once a bubble grows past a predetermined length, the pressure distribution is calculated. A film of coolant is assumed to remain on the pin surface. Both this and the vapour in the bubble are saturated. The vapour film is allowed to move under the influence of vapour drag, wall friction, gravity and axial pressure gradient.

The FCI module treats pin failure into flowing coolant, including both the within the pin phenomena of fuel melting, fission gas release and the ejection of a fuel/fission gas mixture into the coolant channel, as well as the subsequent FCI and voiding of the channel. The model uses a fixed-length time-independent clad rupture and assumes instantaneous mixing throughout the pin cavity of any fuel melted and fission gas released. Fuel motion within the channel is tied to the motion of the coolant slugs defining the interaction zone.

The fission gas voiding model allows some consideration of voiding incoherence by specifying three separate groups of fuel pins in each channel, each with its own different rupture strength. Each group of pins can rupture separately, and the amount of plenum gas left in the pin after rupture is accounted for separately in each pin group. The source of gas is fission gas in the plenum, initial fill gas, and an initial volume of gas in the immediate vicinity of the rupture. This gas has unrestricted access to the coolant channel, while the plenum gas has a user specified impedance to flow. Gas flow continues until the plenum pressure reaches a pre-set value. The gas bubble is assumed to have uniform pressure.

1.2.5 Neutron Kinetics and Reactivity Feedbacks

Once again a point kinetics representation is used, with up to six groups of delayed neutrons. The equations are solved by a modified Runge-Kutta integration method formulated by Kaganove. The method is self-starting, is stable over large integration intervals and has a procedure for changing the integration interval as the nature of the problem changes.

Reactivity feedbacks are taken from the doppler effect, axial fuel expansion (or contraction), coolant density changes and voiding, radial core expansion, fuel slumping and sweepout, and cladding relocation. These are all obtained from input tables of material worth, except for the doppler effect and radial core expansion.

The doppler effect is based on volume averaged fuel temperature change, allowing for coolant voiding and void fraction. The radial expansion feedback is calculated from the temperature change, the linear thermal expansion coefficient for the core structure and an axially weighted displacement-dependent reactivity function.

In SAS3A, the material worths and power distribution are supplied as card input, while for SAS3D a link exists with the D1F3D code. This allows iteration at the steady state between thermal hydraulics, heat transfer and fuel characterisation models on the SAS side and two or three-dimensional neutronics eigenvalue calculations performed by D1F3D. This provides the capability of defining a steady state consistent with regard to local effects, such as control rod cladding, axial temperature and density variations, or assembly by assembly burnup effects.

1.3 CAPRI-2

1.3.1 General Introduction

CAPRI is a German predisassembly code developed in Karlsruhe in 1974 (51). It is similar in its structure to SAS2A, the predecessor of SAS3A. It is set up for up to 30 channels, but can be expanded to any arbitrary number, without much more core requirement.

The code is being used for LOF and TOP analysis of the German SNR 300 reactor.

1.3.2 Cladding, Fuel and Coolant Behaviour

The steady state temperature distribution of fuel, clad, coolant and structure, and fission gas retention pattern are calculated from input data. Changes in fuel and clad dimensions due to thermal expansion, fission gas release and swelling are determined, and the resulting fuel/clad gap size is consistently iterated with the temperature distribution.

After accident initiation fuel and single phase coolant temperatures are calculated simultaneously with one set of equations. The coolant temperatures are determined in an eulerian mesh. Release of fission gas is related to the transient heating of the fuel nodes. Transient induced loads in the fuel and cladding are used together with the fission gas pressure and fuel swelling to calculate radial and axial deformation of the clad and fuel. This is then used to calculate the change in the gap conductance.

During a TOP, clad failure occurs at the time and location where plastic clad deformation and transient clad temperature exceed

threshold values over a specified failure rip length. Another failure criterion used is based on a threshold for the melt fraction again coupled with a cladding temperature limit. Once the clad has failed, FCI is assumed to occur. The model used is a version of the stand-alone fuel sweepout code HOPE. Factors accounted for are: heat transfer from fuel to coolant in the two phase region, fission heating of fuel particles in the coolant channel, motion of fuel in the coolant channel with eventual plate out/blockage or sweepout.

Coolant boiling during a LOF is assumed to start if the maximum coolant temperature exceeds a channel dependent specified superheat. Voiding is then followed with a multiple bubble slug ejection model which takes into account evaporation and condensation of the liquid coolant film including subassembly wall effects.

Slumping is initiated when the fuel melt fraction and the local clad temperature exceed a specified level over a specified number of axial nodes. Collapse of fuel and cladding is calculated with a simple three region model. The movement of the slumping fuel segment into the free channel area is treated as viscous flow. The pin fragments above the molten region lose their support and are allowed to fall towards the core mid-plane.

1.3.3 Neutron Kinetics and Reactivity Feedbacks

The transient reactor power is calculated using the point kinetics module from SAS. Reactivity feedbacks are taken from the doppler effect, fuel and clad axial expansion and density change, fuel

slumping and sweepout, and coolant voiding and density change using channel dependent tables of reactivity worth. The transient power and feedbacks are checked for consistency.

1.4 SURDYN

1.4.1 General Introduction

This is a French code, developed at CEA/CEN (52,53). It is somewhat unusual in that it is used to describe the predisassembly and disassembly phases without discontinuity. In particular voiding and slumping reactivities are calculated in channels not yet undergoing disassembly, whilst in those channels which have reached disassembly, disassembly feedback is being calculated. However, the models used are rather simplistic.

The core is represented by radially concentric annular channels, each containing a single fuel pin. At least twelve channels can be specified. These are divided axially into upper and lower breeder blankets and a central fuel region. The outer channels can be all breeder to represent radial breeder assemblies.

1.4.2 Cladding, Fuel and Coolant Behaviour

No detailed hydraulics or fuel initialisation calculations are carried out, the bulk of data being supplied from input (power, flow and weighting distributions) or from internal tables (material properties).

Boiling starts in a channel as soon as a preset temperature is reached. There is no explicit calculation of coolant and coolant vapour movement during voiding. Once voiding has started, heating of the fuel pin is considered to be adiabatic and heating and melting of the clad are neglected.

Fuel slumping is assumed to start when a set percentage (usually 75%) of the channel fuel cross-sectional area is molten. Molten fuel and the solid fuel above collapse together. Fission gas effects and FCI are not included.

1.4.3 Neutron Kinetics and Reactivity Feedbacks

The neutronics are calculated using a point kinetics module. Reactivity feedbacks are taken from the doppler effect, fuel slumping, and coolant thermal expansion and voiding.

The doppler term accounts for voiding of the channel under consideration. The feedback term from the whole channel is reduced by the ratio of the coolant-out to the coolant-in doppler coefficients for the core.

The slumping feedback is a constant for each channel. It is very simply linked to the difference in fuel worth at the core top and mid-plane, and to the distance fallen by the molten fuel and the solid fuel above.

The sodium temperature reactivity coefficient includes a component for fuel and clad expansion, and is the same for all channels. The feedback term is a linear function of coolant temperature. Once the channel is voided, this term remains constant. Voiding reactivity

is added as a set ramp lasting a set time, starting as soon as voiding is initiated in any channel.

1.5 CARMEN-2

1.5.1 General Introduction

CARMEN-2 is a modular predisassembly code developed by the Belgonucleaire Company in Brussels (54, 55).

The core is divided into ten concentric annular channels. Each channel contains one representative fuel pin and models the upper and lower breeders and the core. The outer channel represents a radial breeder.

1.5.2 Cladding, Fuel and Coolant Behaviour

Slumping of fuel is assumed to begin when a set percentage of the fuel cross-sectional area is molten over a certain axial length, or when the fuel temperature exceeds a certain value. The axial displacements of molten and solid fuel are then calculated using a Lagrangian mesh. If fuel fails in a non-voided region, FCI is considered to start. The model is similar to the Cho and Wright one for the interaction between spherical fuel particles and sodium, with a mixing time constant. The phases of acoustic and inertial constraint are both considered. Heat transfer cut-off due to vapour blanketing can also be modelled.

Channel voiding may be either by FCI or coolant boiling, as fission gas effects are not considered. Boiling starts when a superheat criterion is exceeded. The initial dimensions of the bubble are supplied as input data. A slug ejection model is used which allows the presence of up to two bubbles in a channel at one time. The pressure gradients and vapour motion inside the bubble are calculated, but the motion of the residual coolant film is not. If dry out of the clad occurs, the heat transfer coefficient between the cladding and coolant vapour is assumed to be zero.

1.5.3 Neutron Kinetics and Reactivity Feedbacks

The reactor power is calculated in a point neutron kinetics module. Reactivity feedbacks are taken from the doppler effect, fuel axial expansion, slumping and density change, and coolant voiding and density change. Two dimensional space dependent reactivity coefficients supplied as input data are used in the calculation of the feedback terms. Coolant-in and coolant-out doppler coefficients are given for each channel. Interpolation between these two is carried out for partially voided meshes.

Table 1.1 Engineering Characteristics Favourable to
Loop or Pool Reactors (4)

Pool	Loop
Simple primary circuit boundary: no penetrations below the sodium level.	Primary circuit pipework more accessible.
Primary circuit boundary and core support at constant low temperature.	Mounting of primary pumps and heat exchangers not affected by expansion in primary vessel.
Larger heat sink.	Shorter primary pump shaft.
Easier access to heat sink.	Heat exchangers can have higher pressure drop.
No sodium level control necessary.	Less neutron shielding - no secondary sodium activation problems.
Primary pumps operate cool and at constant temperature.	Smaller quantities of sodium.
Higher porportion of primary pump pressure available for core.	Easier to provide neutronic instrumentation.
Ready provision of fuel storage within primary vessel.	Noise generation (pumps) remote from reactor.
Could tolerate higher core outlet temperature as primary sodium not in contact with containment.	Better suited for very large reactors (above 2000 MW) because of reduced construction problems.

Table 1.2 Loop and Pool Reactors

Reactor	Country	MWt	MWe	Pool or Loop
BN-350 ¹	USSR	1000	350	Loop
PFR ¹	UK	600	250	Pool
PHENIX ¹	France	600	250	Pool
BN-650 ²	USSR	1500	600	Pool
SNR-300 ²	Benelux	730	300	Loop
MONJU ²	Japan	714	300	Loop
CRBR ³	USA	975	350	Loop
CFR ³	UK	3200	1320	Pool
SUPER-PHENIX ²	France	3000	1200	Pool

¹Operating

²Under construction

³Planning

Table 1.3 Comparison of the General Specification of the
Two Cores Considered

	Unit	Core A	Core B
Thermal Power	MW	3150	975
Net Electrical Power	MW	1320	350
Type	-	Pool	Loop
Steam Condition	$^{\circ}\text{C}, \text{bar}$	486,160	482,100
Reactor Inlet Temperature	$^{\circ}\text{C}$	370	388
Reactor Mixed Mean Outlet Temperature	$^{\circ}\text{C}$	540	535
Maximum Fuel Pin Linear Rating	Wcm^{-1}	420	469
Number of Subassemblies	-	342	198
Number of Pins per Subassembly	-	325	217
Subassembly Size (distance across flats)	cm	14.1	11.6
Fuel Pin Outside Diameter	mm	5.84	5.84
Fuel Outside Diameter	mm	5.08	4.91
Cladding Thickness	mm	0.38	0.38
Fuelled Length	cm	100	91.4
Axial Breeder Length	cm	30	35.6
Fission Gas Plenum Length	cm	51(lower)	121.9(upper)
Equivalent Core Diameter	cm	291	187
Primary Sodium Flow Rate	kg.s^{-1}	15000	5223
Fuel Pin Separation	-	Spacer grids	Wire wrap

Table 2.1 Levels of Event and Consequence Seriousness (After 18)

Category	Acceptable Consequences
1. Normal. Events which will occur either because they have been designed to occur or experience has shown to have a high frequency. e.g. Full power operation start ups and shut-downs. Stochiastic fuel failure.	No increase above designated radio-activity levels in restricted containment areas. Accumulated fuel pin cladding strain $<0.1\%$. No increase in plant unavailability.
2. Anticipated Transients. Events expected to occur at least once in plant lifetime. e.g. Loss of off-site power. Scrams. Loss of power to one pump. Operator error.	No increase above designated radio-activity levels in restricted containment areas. Accumulated fuel pin cladding strain $<0.3\%$. Plant unavailability within predictions.
3. Highly unlikely events. Events not expected to occur during plant lifetime, but included in design basis. e.g. Pump seizure Small reactivity addition. Extreme environmental conditions.	No loss of fuel pin cladding integrity or coolable geometry. Downtime for plant repair.

Table 2.1 Levels of Event and Consequence Seriousness (After 18) (Continued)

Category	Acceptable Consequences
4. Hypothetical Events. Events never expected to occur but which are mechanistically possible e.g. Core disruptive accidents.	Loss of plant investment.

Table 2.2 Reactivity Effects of Material Motion

Source of Reactivity	Mechanisms	Reactivity Effect
Fuel Motion i. General	Vapour explosion rupturing one S/A may lead to coherent movement of other S/A's within their clearances causing transient fuel compaction.	Locally positive. Overall effect depends on position.
	Failure of core support may allow core to fall away from control rods.	Positive.
	Portions of pins above failed zone may collapse under gravity.	Positive
	Dispersal of fuel under molten fuel vapour pressure.	Negative, but may be positive in outer zones (autocatalytic effect).
	Seismic shock may cause transient fuel compaction due to movement of S/A's.	Locally positive. Overall effect depends on detailed movement patterns.
ii. Inside clad	Thermal axial expansion of fuel.	Negative.
	Formation of molten fuel/fission gas froth leading to motion of fuel under surface tension and local pressure gradients.	Depends on position of froth formation and direction of fuel motion.

Table 2.2 Reactivity Effects of Material Motion (continued)

Source of Reactivity	Mechanisms	Reactivity Effect
iii. Outside clad.	Slumping of molten fuel down central void under gravity.	Positive.
	Molten fuel driven toward failure point of failed fuel pin.	Depends on failure position.
	Molten fuel either swept out of core by coolant then re-freezes in upper breeder, or falls under gravity to freeze in lower breeder.	Generally negative, but depends on failure position and final location of fuel.
Clad motion	Thermal expansion of cladding.	Positive, but may be negative if clad moves fuel with it.
	Molten clad may be swept out of core by coolant flow or may collapse under gravity.	Positive for sweepout, negative for collapse.
Coolant flow disturbance	Thermal expansion of coolant.	Depends on sodium worth distribution.
	Voiding of coolant from core by boiling, fission gas release or thermal interaction with molten fuel. Voiding rates reduced by large degree of incoherence present.	Positive in central core region, negative in outer.

Table 4.1 Predisassembly Code Comparison

	FRAX (U.K.)	MELT-III/-IIIA (U.S., HEDL)	SAS3A/3D (U.S., ANL)	CAPRI-2 (West Germany)	SURDYN (France)	CARMEN-2 (Belgium)
Incidents treated	LOF/TOP	TOP	LOF/TOP	LOF/TOP	LOF	LOF/TOP
Core Represent- ation.	30 annular channels. Single pin.	20/100 channels, grouped by burnup and power level. Single pin.	10/34 channels, grouped by burnup and power level. Single pin.	30 annular channels. Single pin.	12 annular channels. Single pin.	10 annular Single pin.
Steady state Initial- isation.	From input data and some internal cal- culation.	Direct link to SIEX mat- erial properties code. Includes fuel restructur- ing and f.g. release effects.	Detailed parametric in- ternal thermo-hydraulic and fuel restructuring calculations. Iteration between s.s. temperature distribution and neu- tronics calculation.	Detailed internal thermo-hydraulic and fuel initialisation.	From input data and internal tables.	From input data and some internal cal- culation.

Table 4.1 Predisassembly Code Comparison (continued)

	FRAX (U.K.)	MELT-III/-IIIA (U.S., HEDL)	SAS3A/3D (U.S., ANL)	CAPRI-2 (West Germany)	SURDYN (France)	CARMEN-2 (Belgium)
Pre Failure i Cladding	Axial expansion. Straining due to f.g. pressure and differential expansion.	Residual cladding strength calculated. No explicit calculation of clad motion.	Detailed calculation of clad melting and motion of molten and solid clad in coolant channel. Possible blockage of flow channel. Calculation of clad strain due to f.g. release.	Calculation of clad plastic deformation due to f.g. pressure and differential expansion.	Cladding not explicitly considered.	Cladding not explicitly considered.
ii Fuel	Axial and radial expansion of fuel. Fuel melting.	Axial and radial expansion of fuel. Restructuring and release of f.g. Fuel melting. Effect of fuel/clad gap wire on gap conductance.	Axial and radial expansion of fuel. Restructuring and release of f.g. Fuel melting.	Axial and radial expansion of fuel. Fuel melting.	Molten fuel fraction calculated. No explicit fuel expansion. No f.g. effects.	Melting and axial fuel expansion followed. No f.g. effects.

Table 4.1 Predisassembly Code Comparison (continued)

	FRAX (U.K.)	MELT-III/-IIIA (U.S., HEDL)	SAS3A/3D (U.S., ANL)	CAPRI (West Germany)	SURDYN (France)	CARMEN-2 (Belgium)
iii Coolant	Single bubble slug ejection boiling model. Bubble has uniform pressure.	No boiling model.	Multiple bubble slug ejection model. Motion of coolant film and bubble pressure gradient calculated.	Multiple bubble slug ejection model.	Constant rate voiding once temperature limit exceeded.	Slug ejection boiling model allows 2 bubbles at one time. Pressure gradient and motion of vapour calculated.
Failure Criteria	Clad fails if strain or clad temperature exceeds limits. Fuel collapse if percentage molten fuel exceeds limit.	Clad fails if Damage Parameter (See Appendix I) criterion exceeded. No fuel slumping.	Clad fails if burst strength exceeded by f.g. pressure. Fuel slumping if clad failed and fuel melting reaches unstructured zone.	Clad fails when plastic clad deformation and clad temperature exceed limit. Fuel slumping if percentage molten fuel and clad temperature exceed limit.	No explicit clad failure. Fuel slumping when molten fuel fraction limit exceeded.	No explicit clad failure. Fuel slumping when molten fuel fraction or fuel temperature limit exceeded.

Table 4.1 Predisassembly Code Comparison (Continued)

	FRAX (U.K.)	MELT-III/-IIIA (U.S., HEDL)	SAS/3A/3D (U.S., ANL)	CAPRI (West Germany)	SURDYN (France)	CARMEN-2 (Belgium)
Post Failure						
i. Non voided channel	F.g. voiding or FCI if fuel molten. Same model used as for boiling but with different driving pressure terms. No sweepout or plateout of fuel.	Voiding by FCI. Direct link to HOTPIM code for fuel motion inside pin. Plate out and possible channel blockage considered.	Voiding by FCI or f.g. release. No plate out or channel blockage.	Voiding by FCI. Direct link to HOPE code for fuel motion inside pin and in coolant channel during FCI.	No FCI or f.g. effects considered, only fuel slumping.	Voiding by FCI using Cho-Wright model.
ii. Voided channel	Fuel slumping. Molten and solid fuel and clad above failure fall together under gravity.	No slumping.	Fuel slumping. Detailed compressible hydrodynamics treat slumping region.	Fuel slumping. Motion of slumped fuel treated by simple 3 region model.	Fuel slumping. Treated with very simple model.	Fuel slumping. Detailed calculation of axial motion of molten and solid fuel.

Table 4.1 Predisassembly Code Comparison (Continued)

	FRAX (U.K.)	MELT-III/-IIIA (U.S., HEDL)	SAS/3A/3D (U.S., ANL)	CAPRI (West Germany)	SURDYN (France)	CARMEN-2 (Belgium)
Neutron kinetics and reactivity feedbacks.	Point kinetics with 6 delayed neutron groups. Feedbacks from: doppler effect, fuel expansion and slumping, clad and coolant density and change and coolant voiding.	Point kinetics with 6 delayed neutron groups. Feedbacks from: doppler effect, fuel expansion and motion, core radial expansion, coolant density change and voiding.	Point kinetics with 6 delayed neutron groups. Feedbacks from: doppler effect, fuel expansion and motion, core radial expansion, coolant density change and voiding, clad relocation.	Point kinetics with 6 delayed neutron groups. Feedbacks from: doppler effect, fuel and clad axial expansion and density change, fuel motion, coolant density change and voiding.	Point kinetics with 6 delayed neutron groups. Feedbacks from: doppler effect, fuel slumping, coolant expansion (with contribution for clad and fuel expansion) and voiding.	Point kinetics with 6 delayed neutron groups. Feedbacks from: doppler effect, fuel axial expansion, density change and slumping, coolant density change and voiding.

TABLE 4.2 Comparative LOF Steady State Results (After 22, 23)

		Unit	CAPRI	CARMEN	FRAX	SURDYN	SAS
	Nominal reactor power	M.W.	1947	1932	1971	1964	1947
	Max linear power	Wcm^{-1}	2389	2384	2361	2440	2442
	Peak fuel centre temp.	$^{\circ}\text{C}$	1545	1534	1541	1572	1600
	Peak fuel mean temp.	$^{\circ}\text{C}$	600	601/597	607/605	599.9	611
	Coolant exit temp.	$^{\circ}\text{C}$	450	450	450	450	450
Steady state results for channel 2	Pressure drop above core	bar	0.365	0.452	0.675	-	1.06
	Pressure drop along core	bar	0.871	0.936	0.886	-	0.91
	Pressure drop below core without orifice	bar	1.047	0.862	1.118	-	1.33

Table 4.3 Comparative LOF Transient Results (after 22, 23)

		Unit	CAPRI	CARMEN	FRAX	SURDYN	SAS
Boiling	Time	S	14.65	15.2	15.85	13.66	14.38
Initialisation	Normalised power	-	0.91	0.93	0.88	0.86	0.91
Boiling sequence	Channel number	-	2/3/4/5/7/6/8/9	2/3/4/5/6/7/8/9	2/3/4/5/7/6/8/9	23/4/57/6/8/9	2/3/4/5/7/6/8/9
	Time between first boiling and boiling in Ch.8	S	0.95	0.775	0.71	0.2061	1.31
Boiling results for channel 3	Boiling time till flow reversed	S	0.50	0.57	0.20	-	0.8
	Core mid-plane voiding velocity	ms ⁻¹	3.7	4.85	4.4	-	-
Slumping initiation	Time after boiling initiation	S	1.04	0.601	0.82	0.2193	1.445
	Normalised power	-	148	272	332	127	114

Table 4.3 Comparative LOF Transient Results (after 22, 23) (Continued)

		Unit	CAPRI	CARMEN	FRAX	SURDYN	SAS
Onset of core disassembly	Time	S	15.40	15.926	16.67	13.868	15.38
	Time since start of boiling	S	1.05	0.806	0.826	0.2309	1.45
	Time since start of slumping	msec	6.5	4.4	3	11.6	5
	Normalised power	-	233	261	297	329	200
	Net reactivity and ramp	$\$ \s^{-1}	0.993 -3.4	0.986 6.5	0.984 4.7	1.010 -0.5	0.48 -4.5
	Doppler reactivity and ramp	$\$ \s^{-1}	-1.274 -21.5	-1.330 -23.17	-1.414 -31.5	-1.13 -21.5	-1.25 -20
	Driving reactivity ramp	$\$s^{-1}$	18.1	29.7	36.2	21.0	15.5
	Void reactivity and ramp	$\$ \s^{-1}	2.261 15.8	2.307 25.9	2.396 35.0	1.94 15.0	2.2 15
	Slumping reactivity and ramp	$\$ \s^{-1}	0.006 2.2	0.0094 4.49	0.001 1.1	0.023 4.1	0.002 0.2
End of disassembly	Total energy released at crossover.	M.J.	40610	41000	42100	39300	42000
	Energy released at end of disassembly	M.J.	46105	47660	48370	44500	48510

Table 6.1 Dimensions of Core A and Core B Models

	Units	Core A	Core B
Height of active core	cm	101.0	92.038
Radius of active core	cm	143.704	91.298
Radius of inner enrichment zone	cm	101.273	71.098
Volume of active core	cm ³	6.553×10^6	2.41×10^6
Height of core and axial breeders	cm	222.2	243.46
Radius of core and radial breeders	cm	185.02	142.24
Mass of fuel in active core	gm	1.53×10^7	7.42×10^6
Thermal power output	M.W.	3150	975
Initial average fuel temperature	K	1500	1500

Table 6.2 Smeared Densities for Core A and Core B Models

Zone	Core A		Core B	
	Fuel Density $\rho_F = \text{gm cm}^{-3}$	Total Density $\rho_T \text{ gm cm}^{-3}$	Fuel Density $\rho_F \text{ gm cm}^{-3}$	Total Density $\rho_T \text{ gm cm}^{-3}$
1	2.35	4.62	3.39	5.733
2	2.32	4.55	3.079	5.069
3	2.3	3.85	3.39	5.37
4	2.3	4.68	3.079	5.077
5	2.3	4.62	3.39	5.481
6	2.3	3.95	5.914	7.375
7	2.3	4.82		
8	2.3	4.76		
9	2.3	7.49		
10	2.3	8.46		

Table 6.3 Neutronic Data for Core A and Core B

	Core A	Core B
$\beta_i, \quad i = 1$	7.819×10^{-5}	8.145×10^{-5}
2	6.741×10^{-4}	7.554×10^{-4}
3	5.870×10^{-4}	6.438×10^{-4}
4	1.123×10^{-3}	1.287×10^{-3}
5	4.985×10^{-4}	5.583×10^{-4}
6	1.531×10^{-4}	1.732×10^{-4}
$\beta = \sum \beta_i$	3.1139×10^{-3}	3.50×10^{-3}
$\lambda_i, \quad i = 1$	1.280×10^{-2}	1.290×10^{-2}
2	3.010×10^{-2}	3.120×10^{-2}
3	1.238×10^{-1}	1.338×10^{-1}
4	3.254×10^{-1}	3.450×10^{-1}
5	1.216	1.399
6	2.6971	3.741
Neutron Lifetime	5.0607×10^{-7}	6.000×10^{-7}

Table 6.4 Numbers of Power Peaks During Transient for Core A
and Core B

Ramp Rate	$10s^{-1}$		$20s^{-1}$		$50s^{-1}$		$100s^{-1}$		$200s^{-1}$		$500s^{-1}$		$1000s^{-1}$	
Doppler Constant	A	B	A	B	A	B	A	B	A	B	A	B	A	B
-0.003	1	1	2	2	2	2	1	1	1	1	1	1	1	1
-0.0035	1	1	2	2	2	2	2	1	1	1	1	1	1	1
-0.004	1	1	2	2	2	2	2	2	1	1	1	1	1	1
-0.0045	1	1	2	2	2	2	2	2	1	1	1	1	1	1
-0.005	1	1	2	2	2	2	2	2	2	1	1	1	1	1
-0.0055	1	1	1	2	3	2	2	2	2	2	1	1	1	1
-0.006	1	1	1	2	3	3	2	2	2	2	1	1	1	1
-0.0065	1	1	1	2	3	3	2	2	2	2	1	1	1	1

	Total Energy Deposited		Peak Energy Density		Peak Temperature, Pressure		Average Temperature	
	G.J.		kJ gm ⁻¹		deg K, bar		deg K	
	Core A	Core B	Core A	Core B	Core A	Core B	Core A ¹	Core B ²
\$10s ⁻¹ d = -0.005	16.54	7.738	1.511	1.503	4175, 11.4	41591, 10.8	3263	3299
\$100s ⁻¹ d = -0.005	25.01	13.11	2.284	2.547	5586, 302	5984, 547	4250	4577
\$1000s ⁻¹ d = -0.005	63.20	35.94	5.773	6.982	18470, 13704	24211, 19686	10706	15027

¹Flux-squared weighted

²Mass-weighted

Table 6.5 Final Peak and Average Temperatures and Peak Pressures for
Cores A and B

Table 6.6 Amount of Reactivity Inserted by Final
Power Peak

Ramp Rate $\$s^{-1}$	Amount of Reactivity Inserted, \$		
	$d=-0.004$	$d=-0.005$	$d=-0.006$
10	1.87	2.09	2.28
20	1.92	2.12	2.32
50	1.95	2.20	2.40
100	2.10	2.27	2.40
200	1.80	2.40	2.80
500	2.50	2.50	2.50
1000	3.00	3.00	3.00

Table 6.7 Excursion Yields with Limited Reactivity
Input

Amount of Reactivity Available at $\$100s^{-1}$, \$	Excursion Yield M.J.	Thermal Yield G.J.
No limit	895.6	25.01
3.0	895.6	25.01
2.5	894.6	25.00
2.275	651.3	23.54
2.25	569.6	22.98
2.0	6.191	15.25
1.5	0.0	11.183
1.0	Transient not complete after 2.5 sec	

Table 6.8 Piecewise Linear Reactivity Input - $\$100s^{-1}$

Ramp Increased to $\$500s^{-1}$. Doppler Coefficient

$$d = -0.005$$

Time of Increase from Start of Transient msec	Excursion Yield M.J.	Thermal Yield G.J.
0	4141.0	37.27
2.5	4154.0	37.31
5	4168.0	37.35
7.5	4159.0	37.32
10	3968.0	36.75
12	3508.0	35.33
14	4003.0	37.03
15	5553.0	41.30
16	7305.0	45.96
17	8593.0	49.25
18	9268.0	50.93
19	9378.0	51.21
20	8752.0	49.65
21	7919.0	47.54
22	6013.0	42.55
23	3372.0	34.89
24	1140.0	26.31
25	901.7	25.05
∞	895.6	25.01

Table 6.9 Influence of Fission Products on Final Peak

Fuel Temperatures and Final Peak Vapour Pressures

Ramp Rate s^{-1}	No Fission Products Pressure bar Temperature K	$\frac{1}{4}$ Brook '18' bar K	Brook '18' bar K	Brook '10' bar K
10	11.42 4175	-	-	0.0034 3099.95
50	70.21 4833	28.94 4482	5.52 3960	0.369 3358
100	301.8 5586	268.7 5519	175.5 5276	36.2 4566
500	1959.0 7216	1932.0 7190	1577.0 7006	1290.0 6630

In all cases doppler constant = -0.005

Ramp Rate \$s ⁻¹	No Fission Products		$\frac{1}{4}$ Brook '18' Fission Products		Brook '18' Fission Products		Brook '10' Fission Products	
	No Radial Restraint M.J.	Radial Restraint M.J.	No Radial Restraint M.J.	Radial Restraint M.J.	No Radial Restraint M.J.	Radial Restraint M.J.	No Radial Restraint M.J.	Radial Restraint M.J.
10	28	49	0	0.05	0	0	0	0
50	240	298	94	181	8	45	0	0
100	896	984	813	934	562	750	121	307
500	4121	4567	4102	4555	3960	4452	3304	3936

In all cases doppler coefficient = -0.005

Table 6.10 Effect of Fission Products and Radial Restraint on
Excursion Yield

Ramp Rate s ⁻¹	Threshold Pressure									
	0 bar		10 bar		50 bar		100 bar		1000 bar	
	No Radial Restraint	Radial Restraint	No Radial Restraint	Radial Restraint	No Radial Restraint	Radial Restraint	No Radial Restraint	Radial Restraint	No Radial Restraint	Radial Restraint
	M.J.	M.J.	M.J.	M.J.	M.J.	M.J.	M.J.	M.J.	M.J.	M.J.
10	28	49	152 (95) ¹	192 (133)	415 (141)	473 (197)	639 (151)	712 (210)	3298 (344)	3448 455
50	240	298	332 (282)	398 (352)	661 (387)	849 (571)	1228 (755)	1478 (1062)	4193 (1166)	4455 (1490)
100	896	984	951 (908)	1026 (988)	1042 (805)	1104 (847)	1122 (650)	1184 (728)	7037 (4705)	7563 (5287)

¹Yield calculated only from meshes above Threshold Pressure.

In all cases doppler coefficient = -0.005

Table 6.11 Effect of Threshold Pressure on Excursion Yield

Initial Power ($P_0 = 975$ M.W.)	Peak Relative Power p/p_0	Peak Reactivity β	Final Average Temperature K	Thermal Yield G.J.
p_0	2405 ¹ 5508 ²	1.137 ¹ 1.253 ²	4238 ¹ 5112 ²	11.74 ¹ 10.08 ²
50 p_0	1090 586	0.984 0.935	3980 3975	10.72 5.58
100 p_0	714 346	0.868 0.720	3916 3946	10.47 5.47
200 p_0	470 368	0.602 0.460	3915 4060	10.46 5.88

Ramp Rate = $50s^{-1}$

¹Initial temperature 1500K

²Initial temperature 3000K

Table 6.12 Initial Power Parametrics

Initial Reactivity \$	Peak Relative Power P/P _O	Peak Reactivity \$	Final Average Temperature K	Thermal G.J.
0.0	2405 ¹ 5508 ²	1.137 ¹ 1.253 ²	4238 ¹ 5112 ²	11.74 ¹ 10.08 ²
0.95	2445 8721	1.249 1.327	4437 5486	12.54 11.83
1.00	2895 9214	1.269 1.340	4448 5554	12.58 12.17
1.05	2791 9394	1.292 1.363	4438 5635	12.54 12.59

Ramp Rate = \$50s⁻¹ P_O = 975 M.W.

¹Initial temperature 1500K

²Initial temperature 3000K.

Table 6.13 Initial Reactivity Parametrics

Initial Precursor Density	Peak Relative Power p/p_0	Peak Reactivity ρ	Final Average Temperature K	Thermal Yield G.J.
C_0	2445	1.249	4437	12.54
$0.01C_0$	2917	1.266	4447	12.58
$0.0001C_0$	2830	1.272	4447	12.58
$0.000001C_0$	2829	1.272	4447	12.58
$0.00000001C_0$	2829	1.272	4447	12.58

Initial Power P_0 , Initial Reactivity $\rho 0.95$, Initial Temperature 1500K

C_0 = Equilibrium precursor densities.

Table 6.14 Initial Delayed Neutron Precursor Parametrics

Table 6.15 Equilibrium and Crossover Delayed Neutron
Precursor Densities

Neutron Group	Equilibrium	Crossover
1	1.0260×10^{20}	1.0297×10^{20}
2	3.9344×10^{20}	4.3379×10^{20}
3	7.8189×10^{19}	1.3020×10^{20}
4	6.0620×10^{19}	1.6918×10^{20}
5	6.4849×10^{18}	4.7303×10^{19}
6	7.5254×10^{17}	1.0580×10^{19}

Initial Power $P_0 = 975 \text{ M.W.}$	Initial Reactivity \$	Peak Relative Power		Peak Reactivity		Final Average Fuel Temperature		Thermal Yield		Change in Yield $\frac{\text{EXTRA-VENUS}}{\text{VENUS}} \times 100\%$
		VENUS ¹ P/P ₀	EXTRA P/P ₀	VENUS ¹ \$	EXTRA \$	VENUS ¹ K	EXTRA K	VENUS ¹ G.J.	EXTRA G.J.	
50P ₀	0.95	3040	3806	1.15	1.18	4644	4787	7.510	8.688	+16
50P ₀	1.00	3176	3887	1.16	1.19	4673	4823	7.611	8.837	+16
50P ₀	1.05	3505	4372	1.17	1.20	4748	4899	7.862	9.155	+16
100P ₀	0.95	2711	3344	1.14	1.16	4561	4698	7.239	8.324	+15
200P ₀	0.95	2245	2854	1.12	1.14	4436	4564	6.824	7.785	+14
100P ₀	1.05	3043	3999	1.15	1.18	4636	4796	7.486	8.723	+17
200P ₀	1.05	2495	3328	1.12	1.15	4487	4663	6.991	8.181	+17

Ramp Rate 50s^{-1}

¹Meyer et al (37)

Table 6.16 Comparison of Initial Power and Reactivity Parametrics

Initial Power $P_0 = 975 \text{ M.W.}$	Initial Reactivity \$	Ramp Rate $\text{\$/s}^{-1}$	Peak Relative Power		Peak Reactivity		Final Average Fuel Temperature		Thermal Yield		Change in Yield $\frac{\text{EXTRA}-\text{VENUS}}{\text{VENUS}} \times 100\%$
			VENUS ¹ P/P_0	EXTRA P/P_0	VENUS ¹ \$	EXTRA \$	VENUS ¹ K	EXTRA K	VENUS ¹ G.J.	EXTRA G.J.	
$50P_0$	0.95	50	3040	3806	1.15	1.18	4644	4787	7.510	8.688	+16
$50P_0$	0.95	100	7275	8104	1.28	1.31	5622	5558	10.690	12.19	+14
$50P_0$	1.05	50	3505	4372	1.17	1.20	4748	4899	7.862	9.155	+16
$50P_0$	1.05	100	7726	9366	1.29	1.32	5694	5625	10.920	12.54	+15
$200P_0$	1.05	50	2495	3328	1.12	1.15	4487	4663	6.991	8.181	+17
$200P_0$	1.05	100	5678	6977	1.23	1.25	5332	5339	9.768	11.12	+14

¹Meyer et al (37)

Table 6.17 Comparison of Ramp Rate Parametrics

Table 6.18 Comparison of Initial Parameter Variation
Surveys

Parameter	Change in Parameter			Change in Thermal Yield		
	VENUS ¹	VENUS ²	EXTRA	VENUS ¹	VENUS ²	EXTRA
Initial Power	0.5p _O	0.5p _O	0.5p _O	1.05E _O	1.04E _O	1.04E _O
	2.0p _O	2.0p _O	2.0p _O	1.06E _O	0.94E _O	0.94E _O
Initial Reactivity	1.06ρ _O	1.10ρ _O	1.10ρ _O	1.16E _O	1.02E _O	1.05E _O
Ramp Rate	0.5R _O	0.5R _O	0.5R _O	0.83E _O	0.70E _O	0.71E _O
	1.5R _O	-	2.0R _O	1.18E _O	-	1.42E _O
Doppler Coefficient	0.5d _O	0.83d _O	0.79d _O	1.32E _O	1.10E _O	1.12E _O
	2.0d _O	1.20d _O	1.19d _O	0.63E _O	0.90E _O	0.91E _O
Neutron Lifetime	0.5ℓ*	-	0.83ℓ*	0.94E _O	-	0.97E _O
	2.0ℓ*	-	1.17ℓ*	1.05E _O	-	1.02E _O

¹Nicholson and Jackson (44)

²Meyer et al (37)

p_O, ρ_O, R_O, d_O: Base case values of relevant parameter.

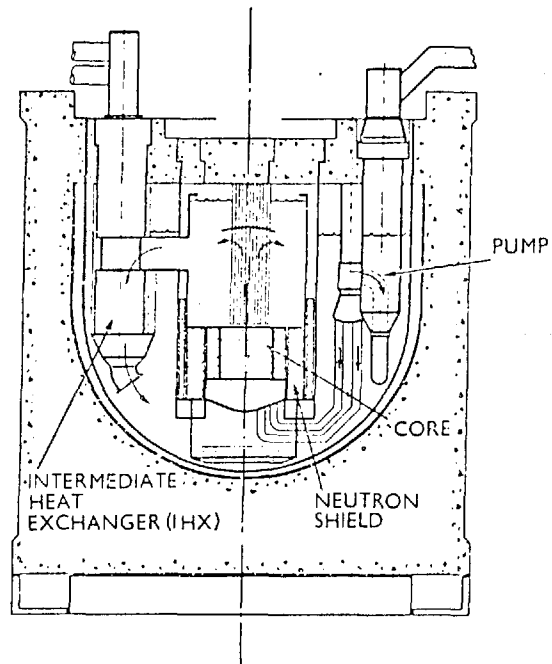


Fig. 1.1a Pool Configuration

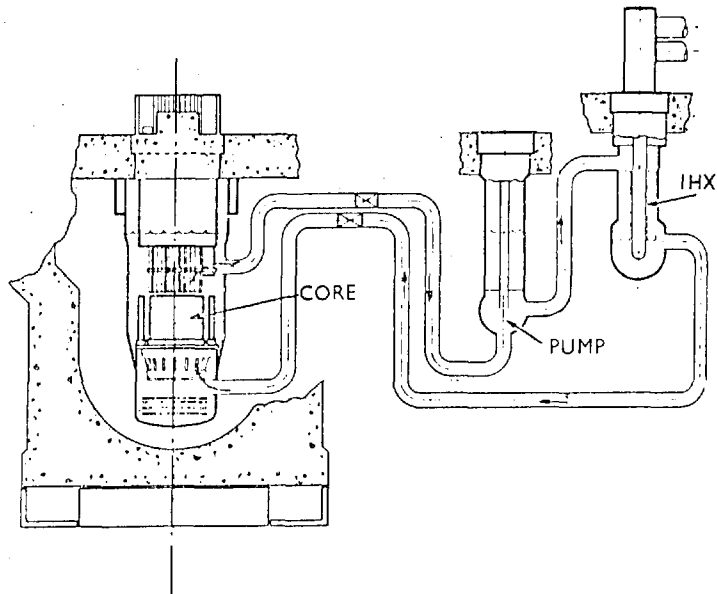


Fig. 1.1b Loop Configuration

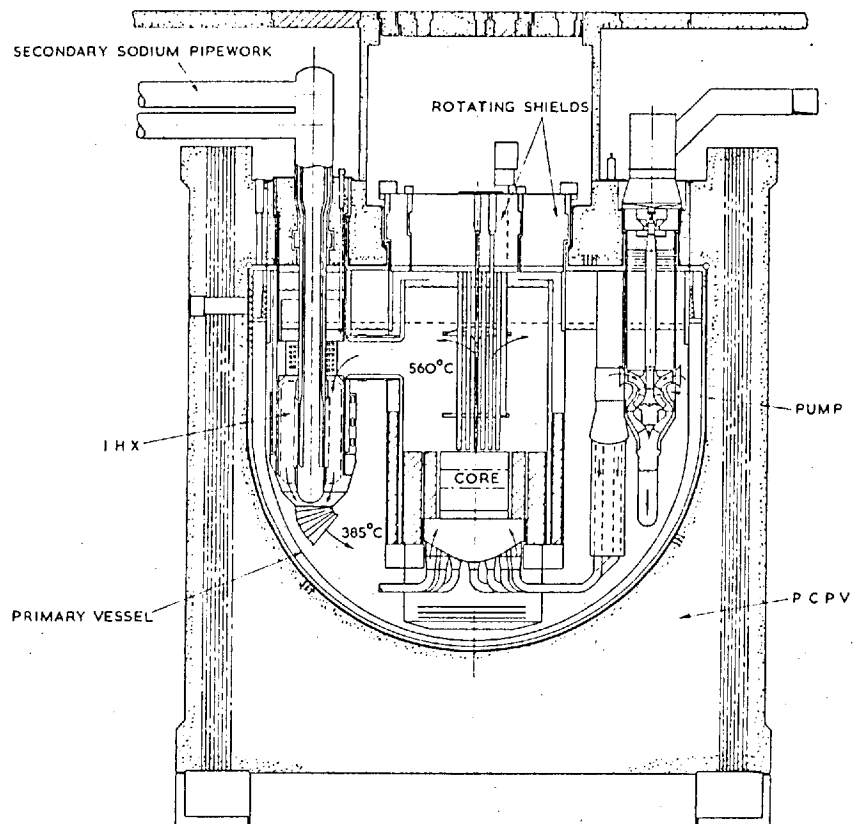
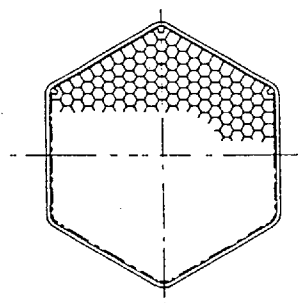
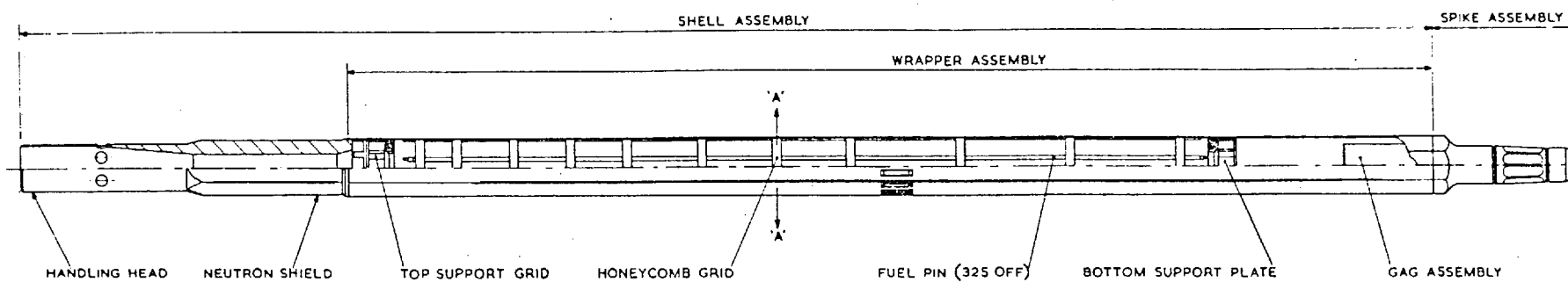
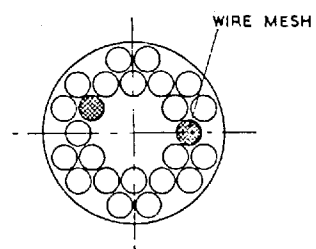


Fig. 1.2 Section of a Typical Pool Reactor



SECTION 'A-A'
SHOWING PART OF A TYPICAL
HONEYCOMB GRID IN WRAPPER



TYPICAL GAG PLATE

Fig. 1.3 Typical Subassembly
(After 5)

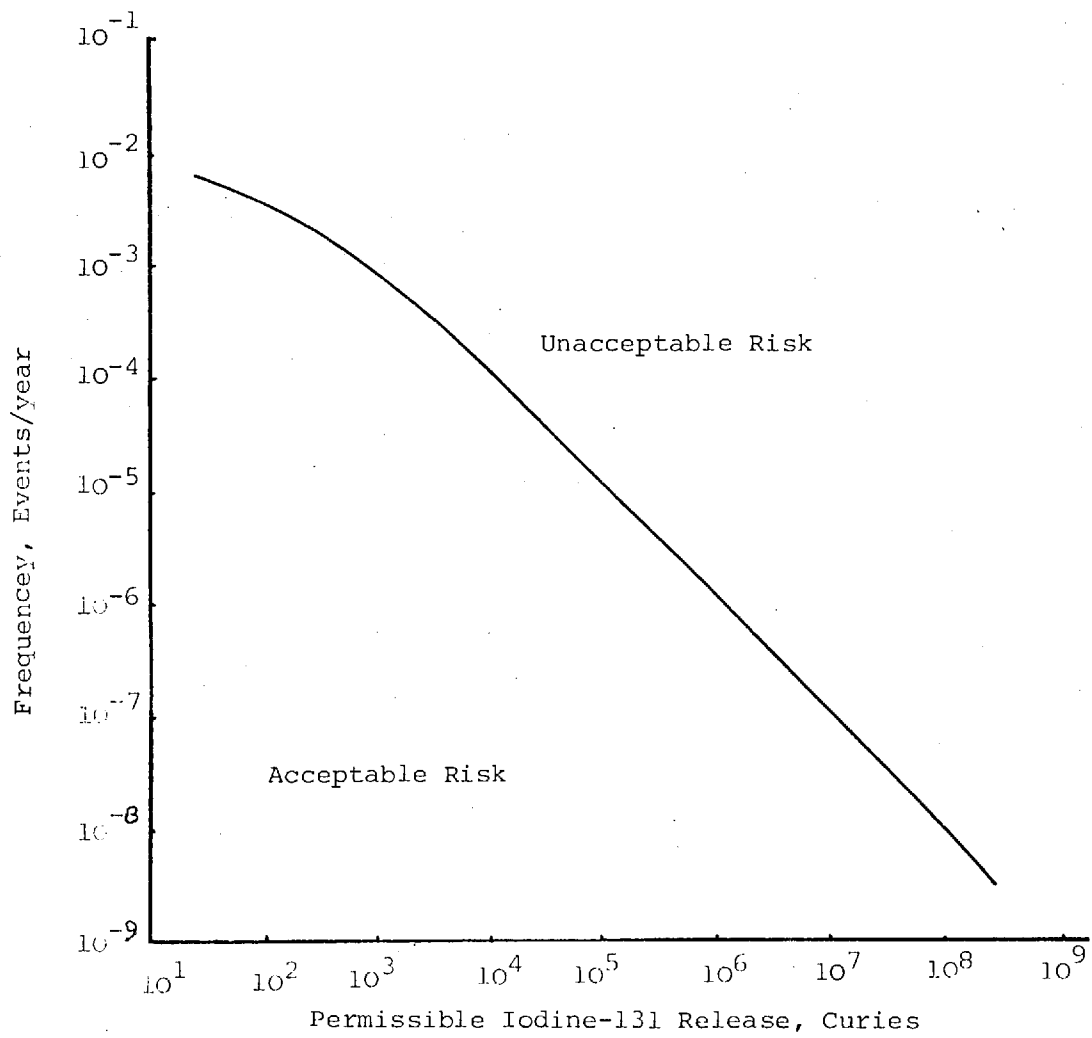
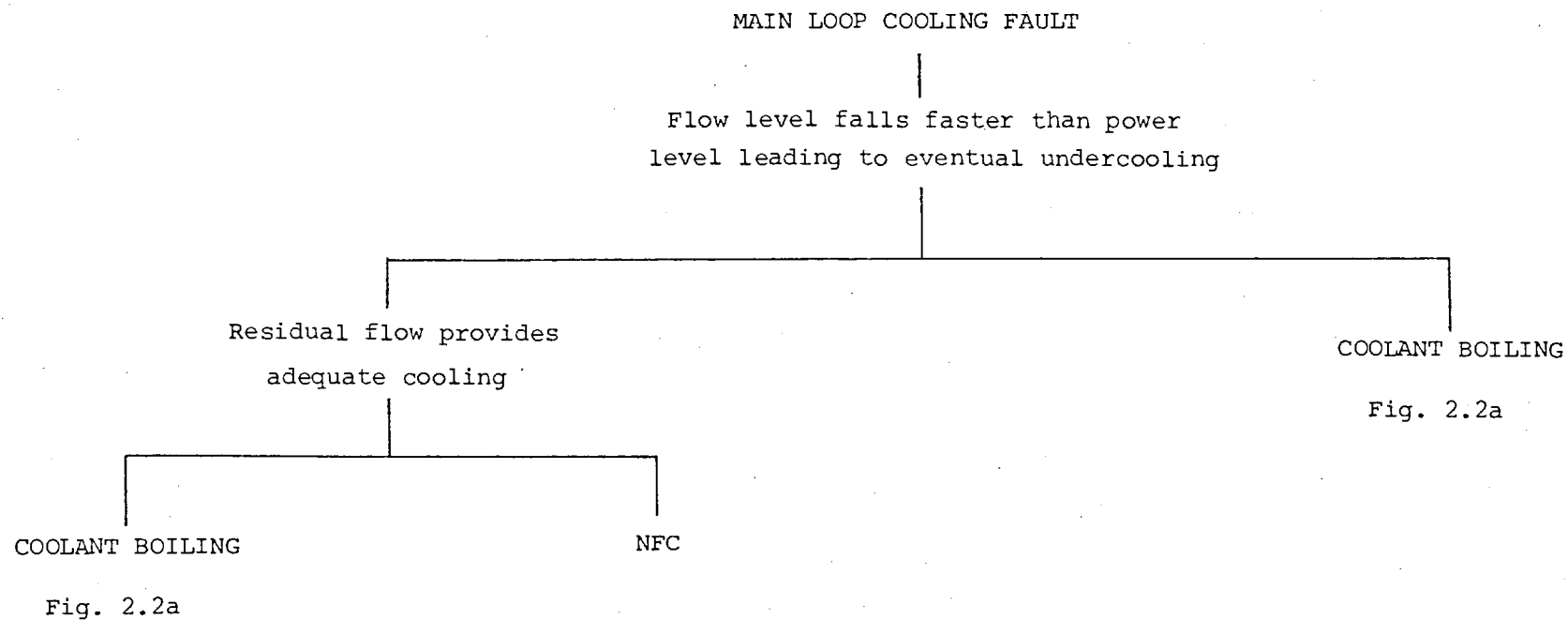


Fig. 2.1 Example Safety Criterion (After 14)



NFC - No Further Consequences

SRE - Severe Reactivity Excursion

PAHR - Post Accident Heat Removal

In all cases:

Branch to right - YES

Branch to left - NO

Fig. 2.2 Loss of Flow-Main Tree

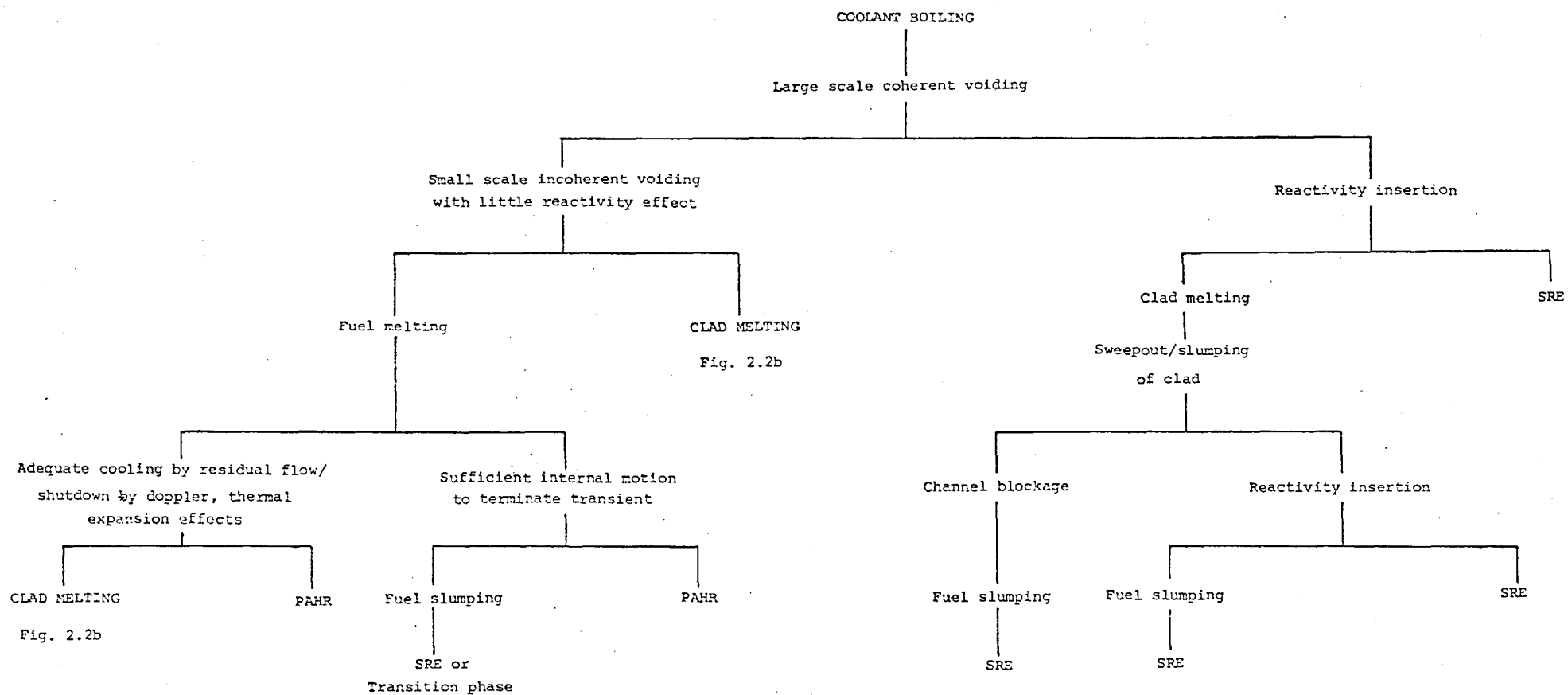


Fig. 2.2a Loss of Flow - Coolant Boiling Sub-Tree

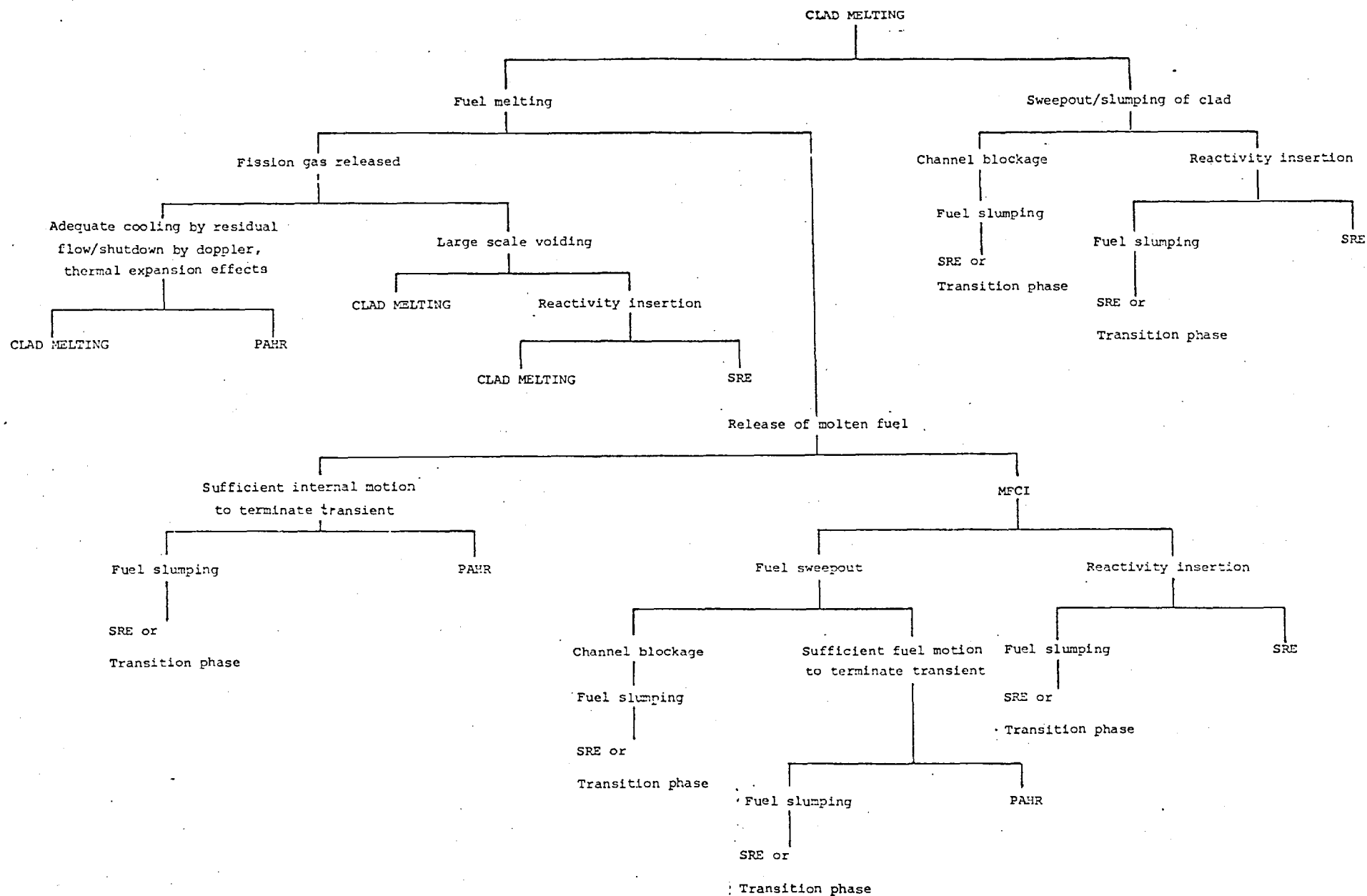


Fig. 2.2b Loss of Flow - Clad Melting Sub-Tree

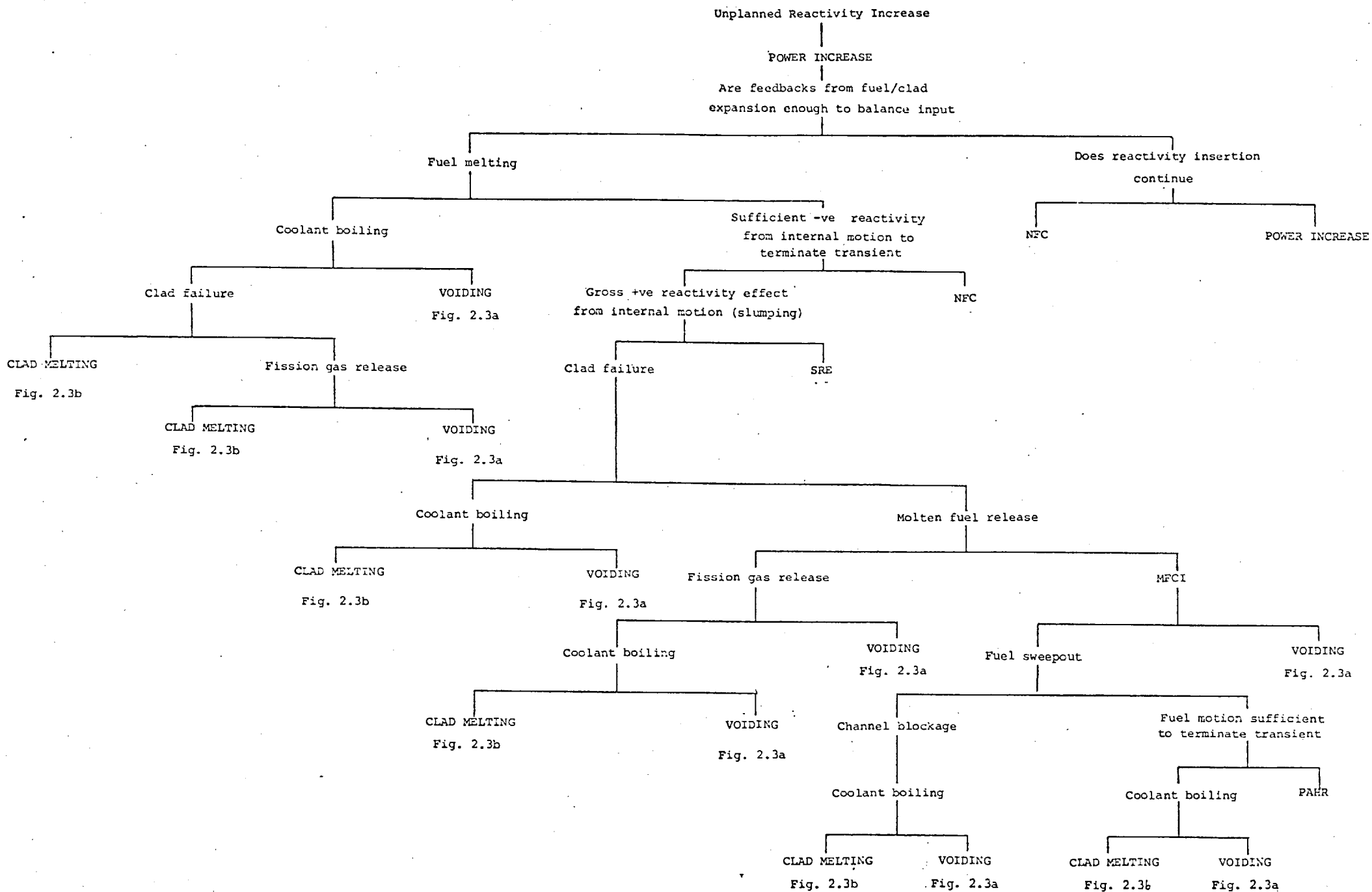


Fig. 2.3 Transient Overpower - Main Tree

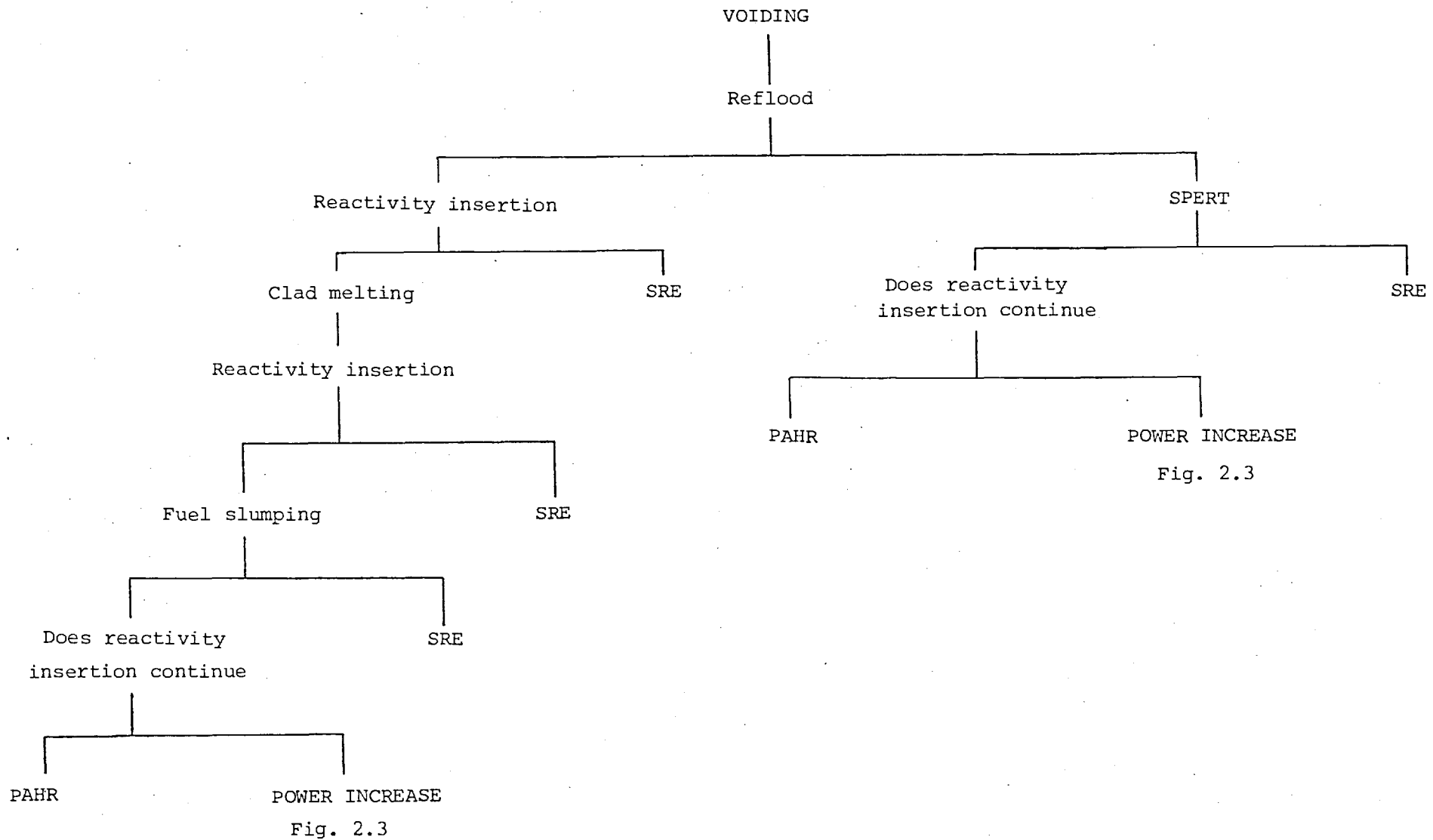


Fig. 2.3a Transient Overpower - Voiding Sub-Tree

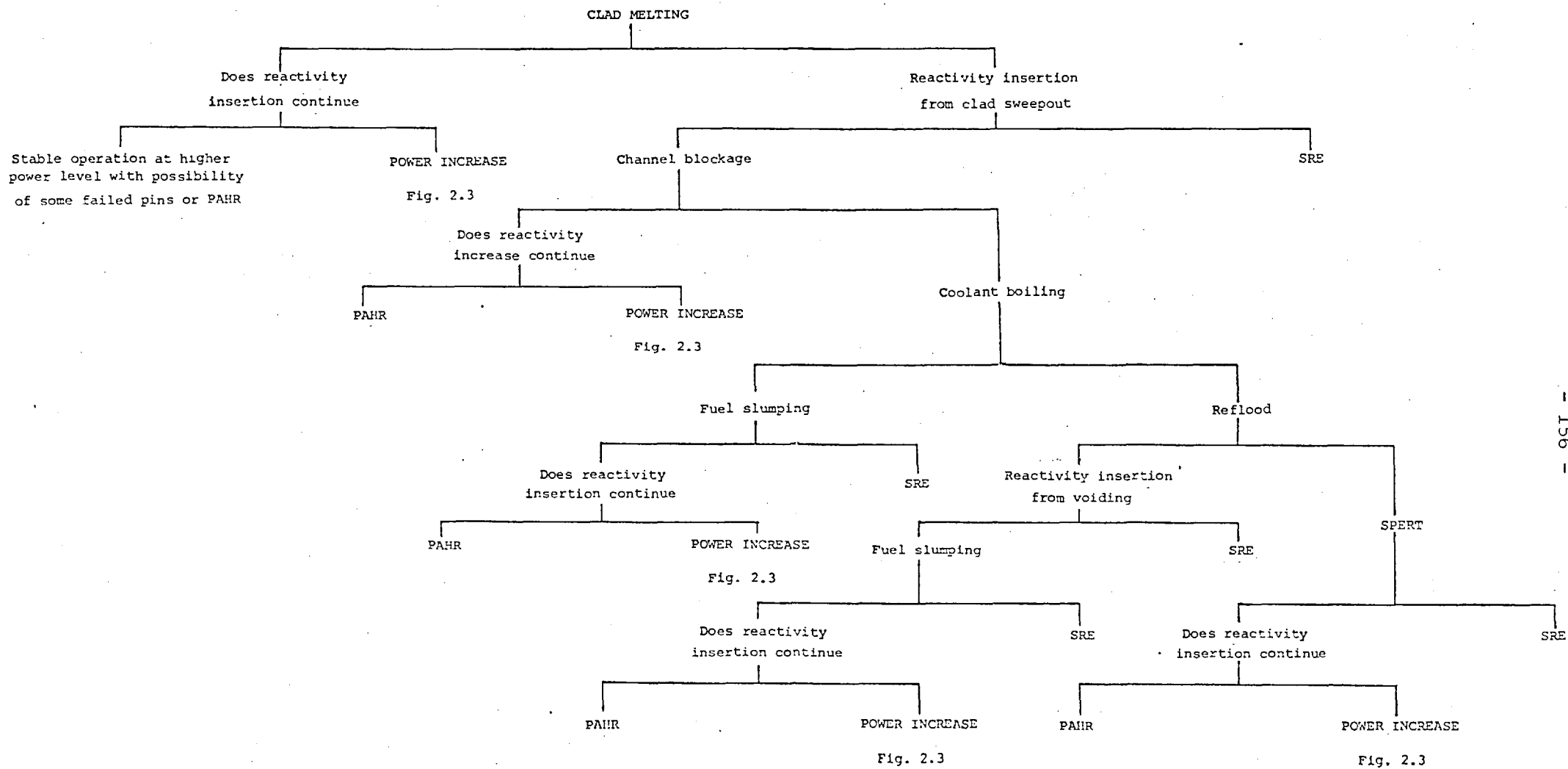


Fig. 2.3b Transient Overpower - Clad Melting Sub-Tree

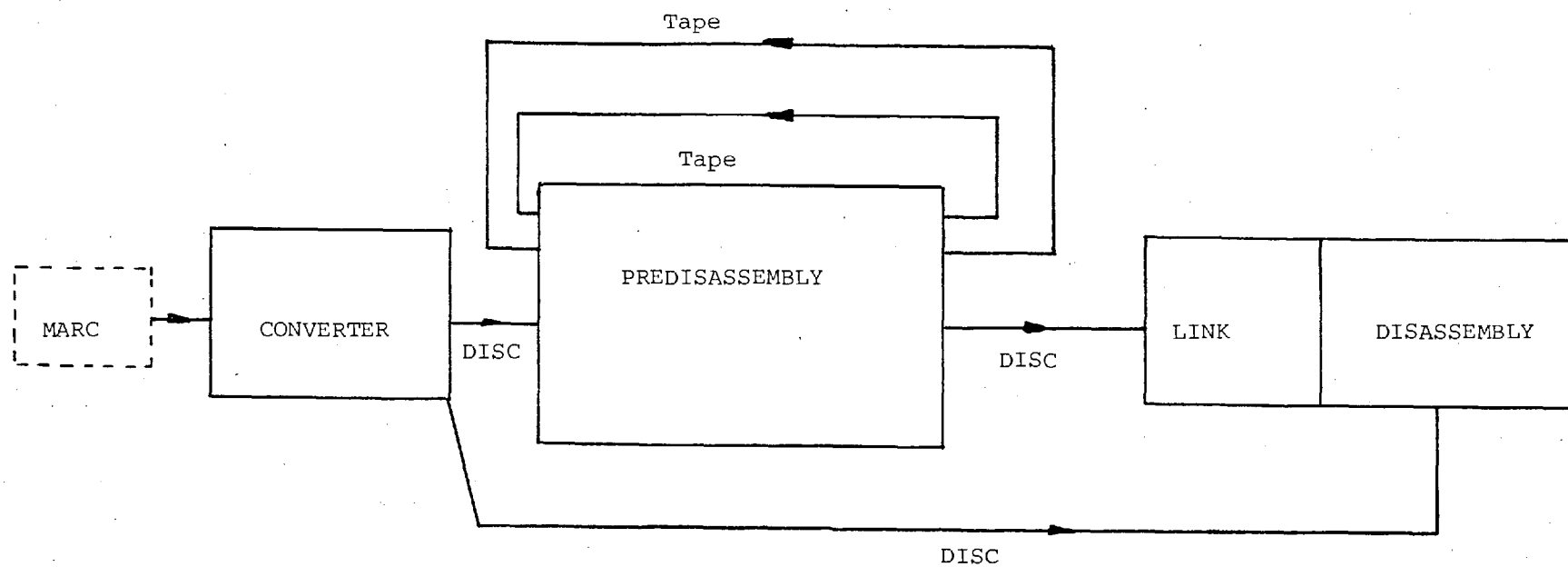
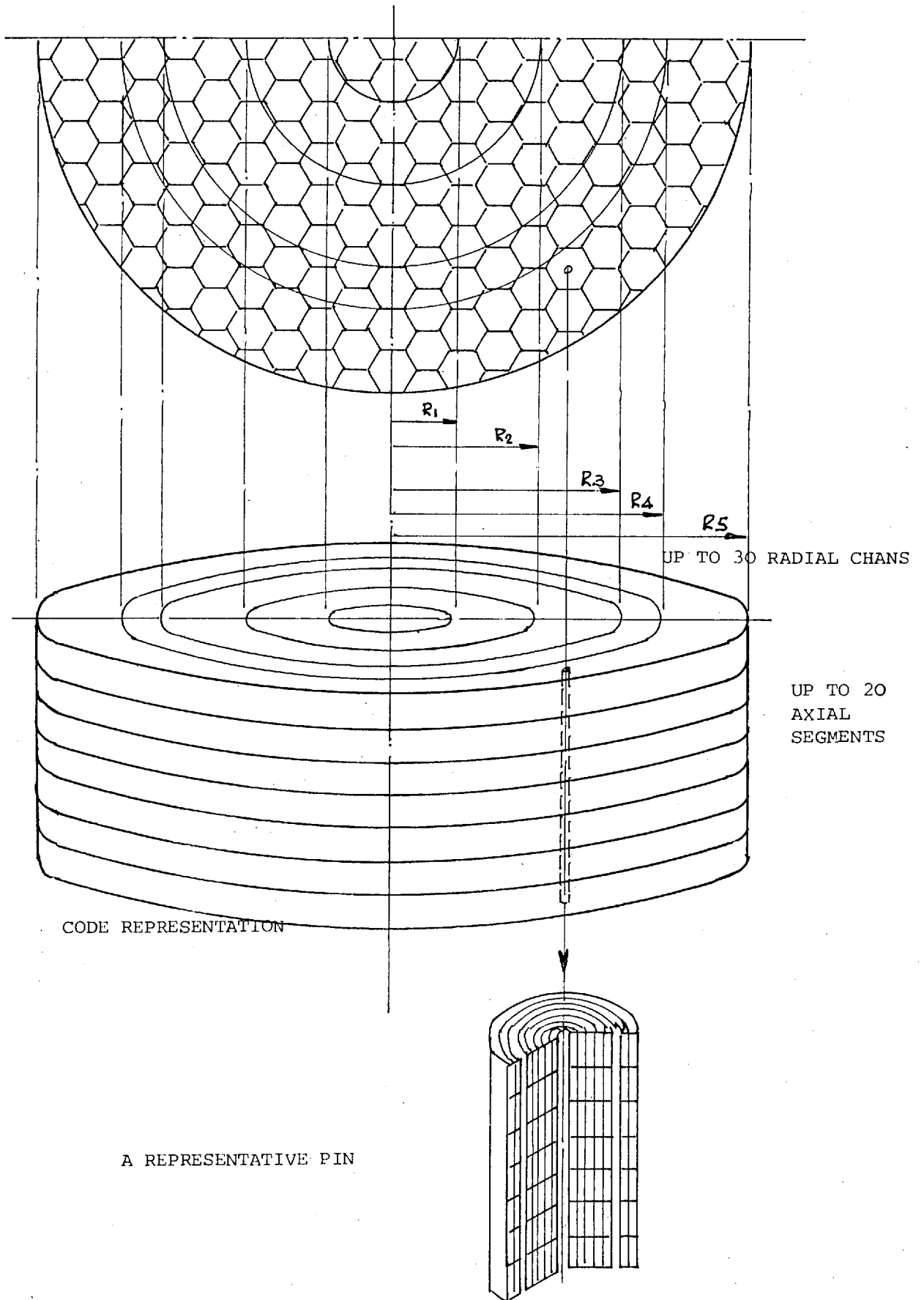


Fig. 3.1 Schematic Layout of the FRAX Predisassembly Code Package

Figure 3.2 Schematic of FRAX code Representation



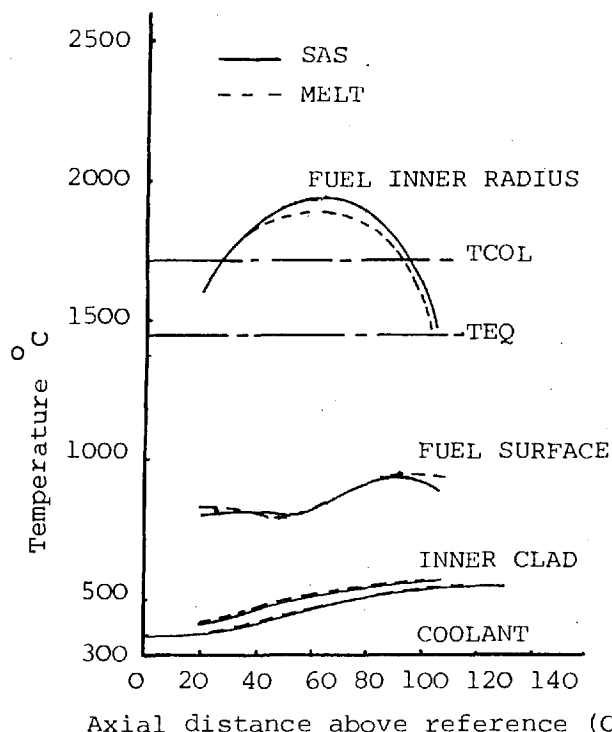


Fig. 4.1 Steady State Axial Temperature Profiles (After 21)

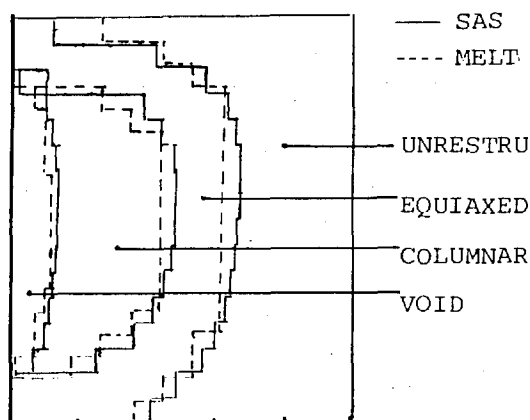


Fig. 4.2 Fuel Restructuring Patterns (after 21)

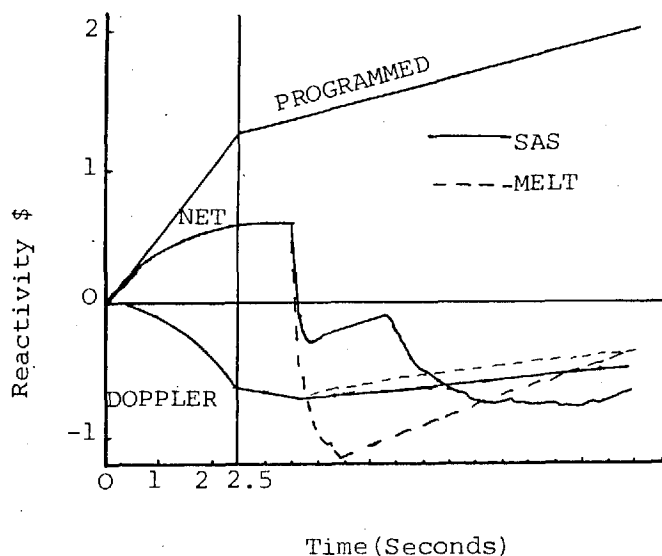


Fig. 4.3 Net and Doppler Feedbacks (After 21)

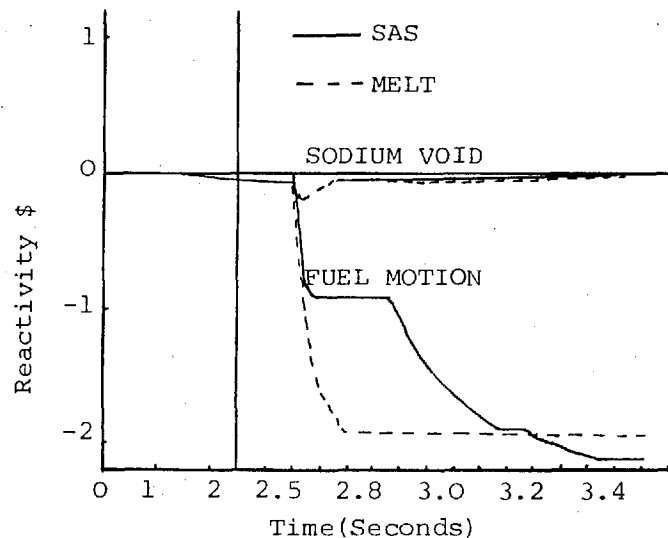


Fig. 4.4 Sodium Void and Fuel Motion Feedbacks (After 21)

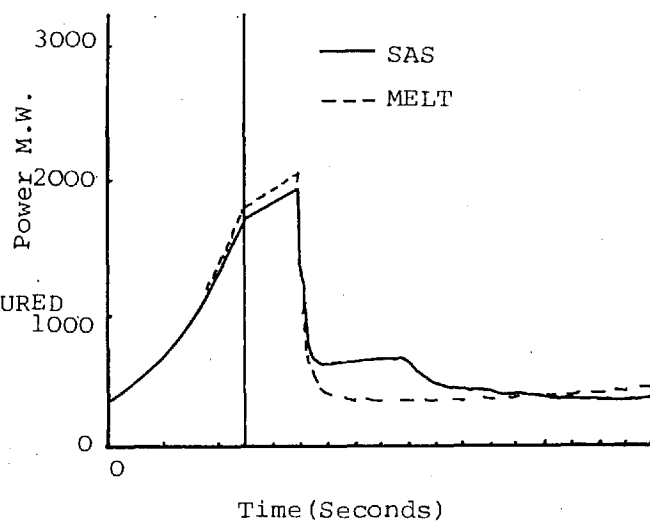


Fig. 4.5 Power Traces (After 21)

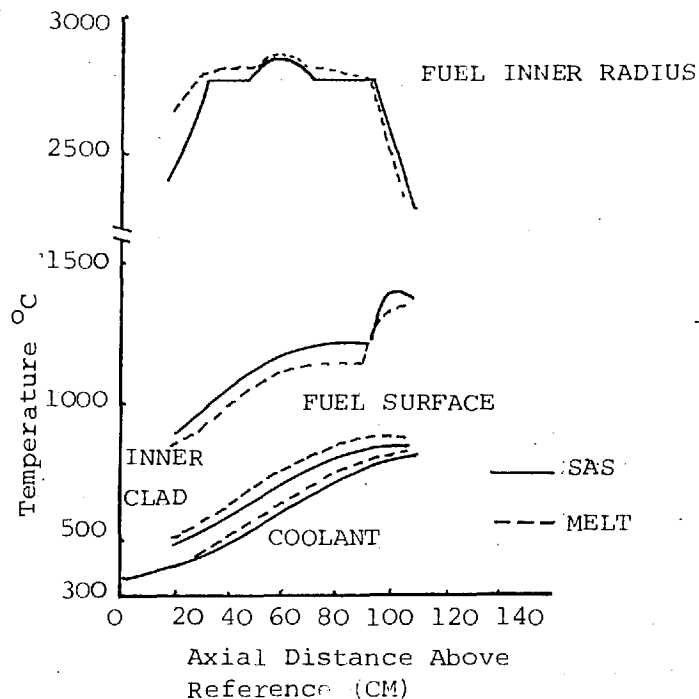


Fig. 4.6 Axial Temperature Profiles (2.68sec) (After 21)

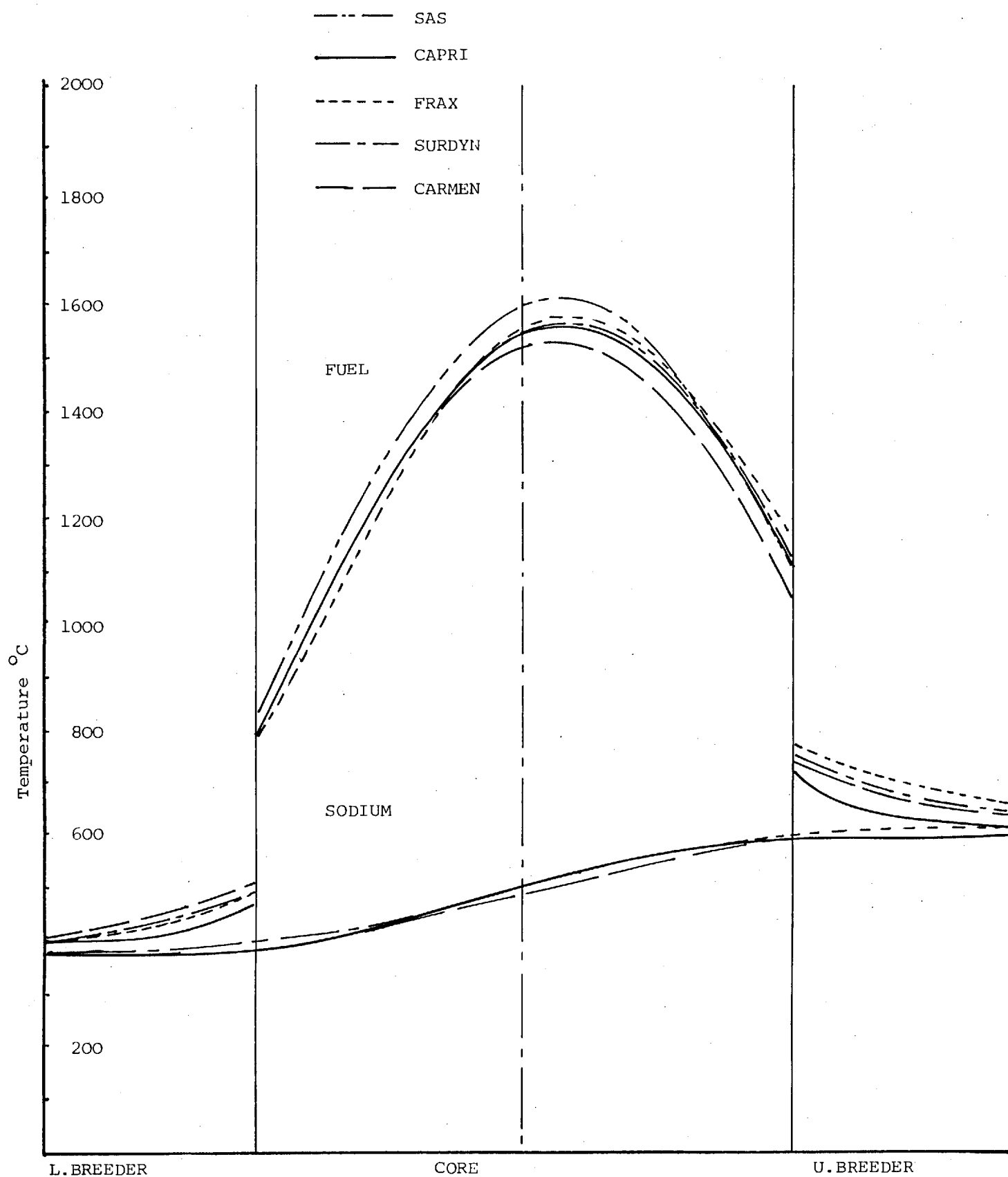


Fig. 4.7 Average Fuel and Sodium Steady State Axial Temperature Distribution in Channel 2 (After 22,23)

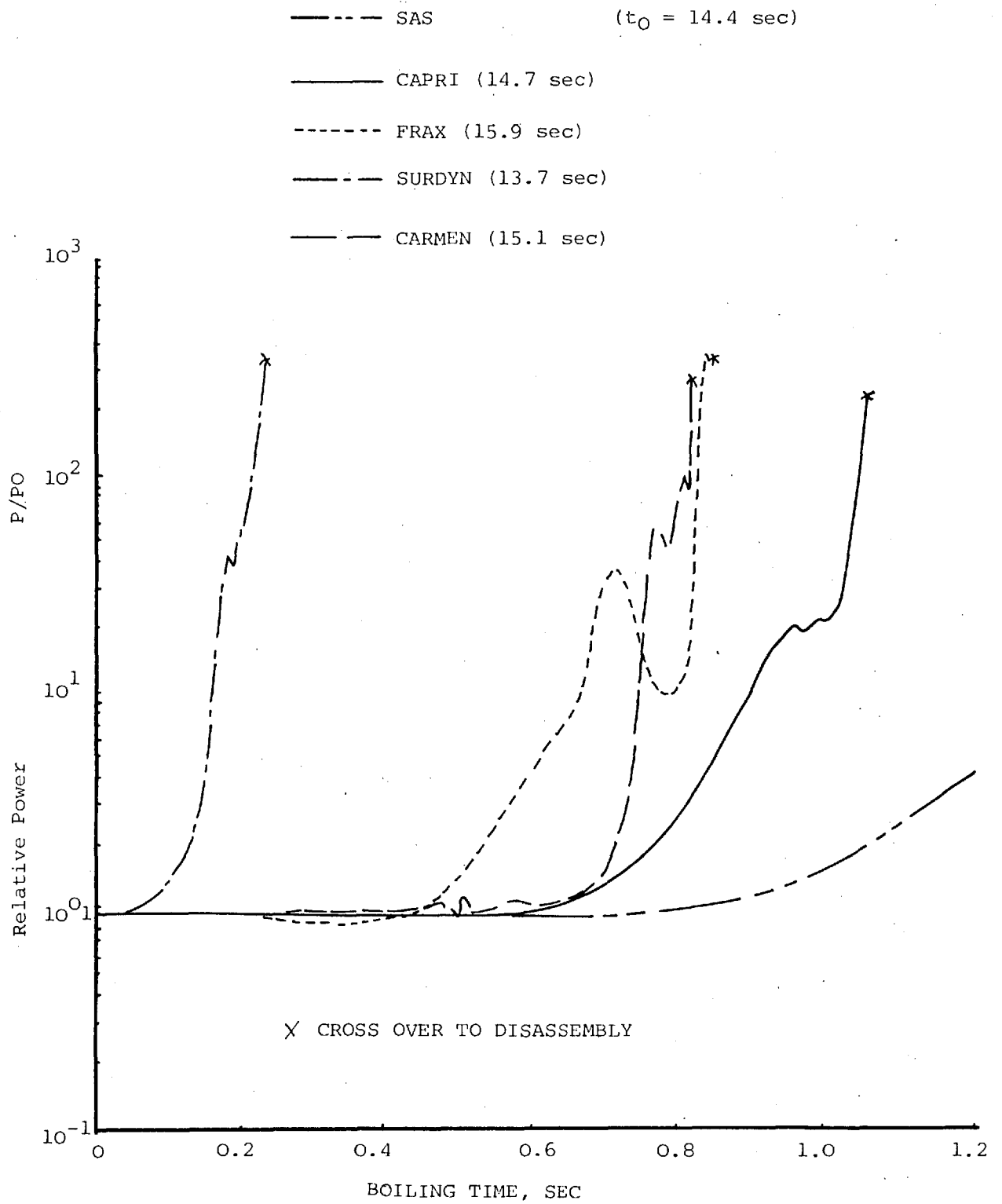


Fig. 4.8 Relative Power P/PO vs. Time After First Boiling Initiation till Onset of Disassembly (After 22,23)

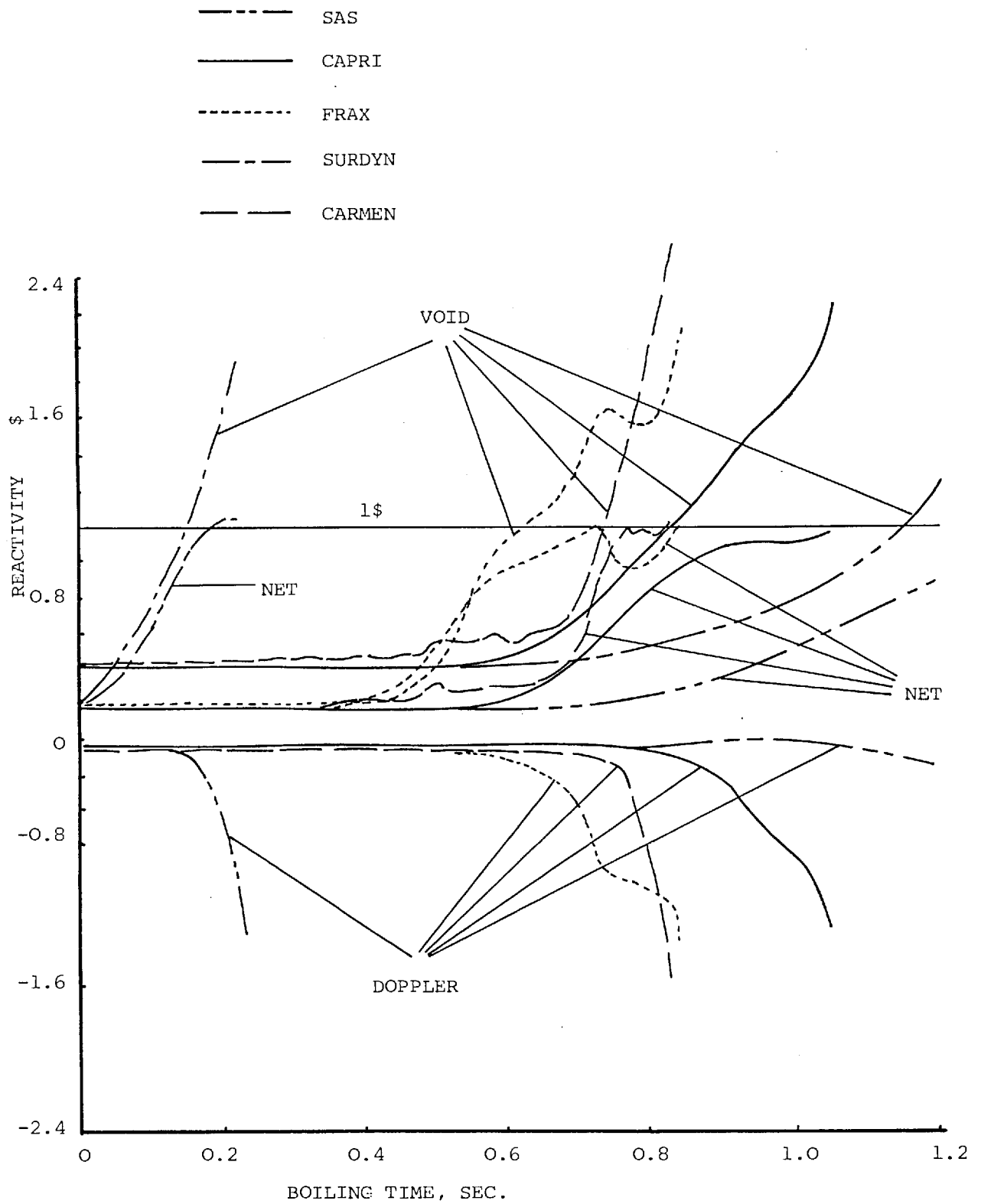
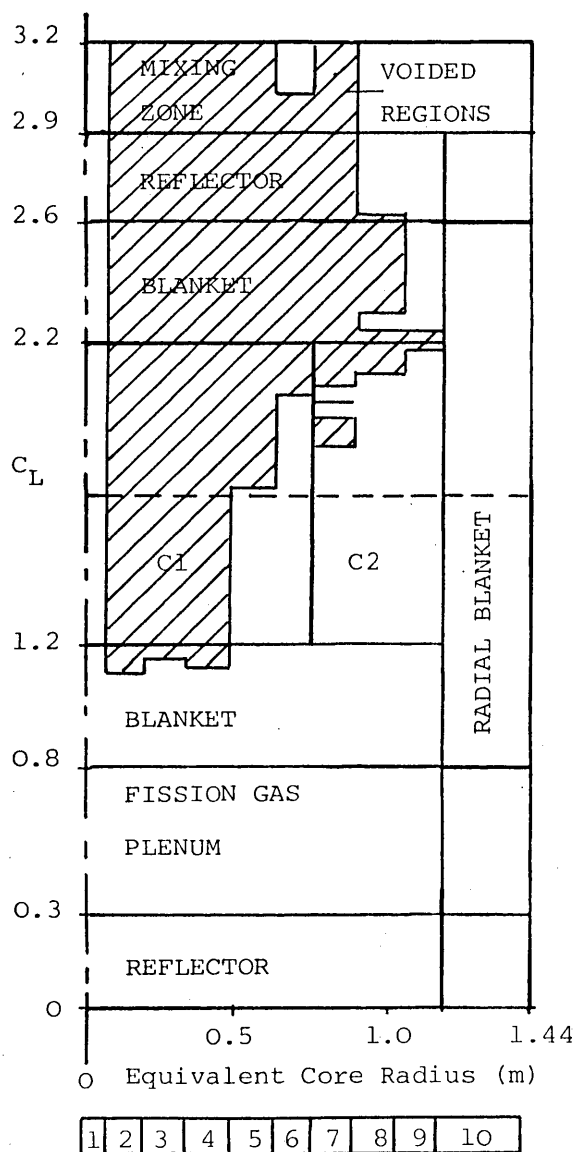
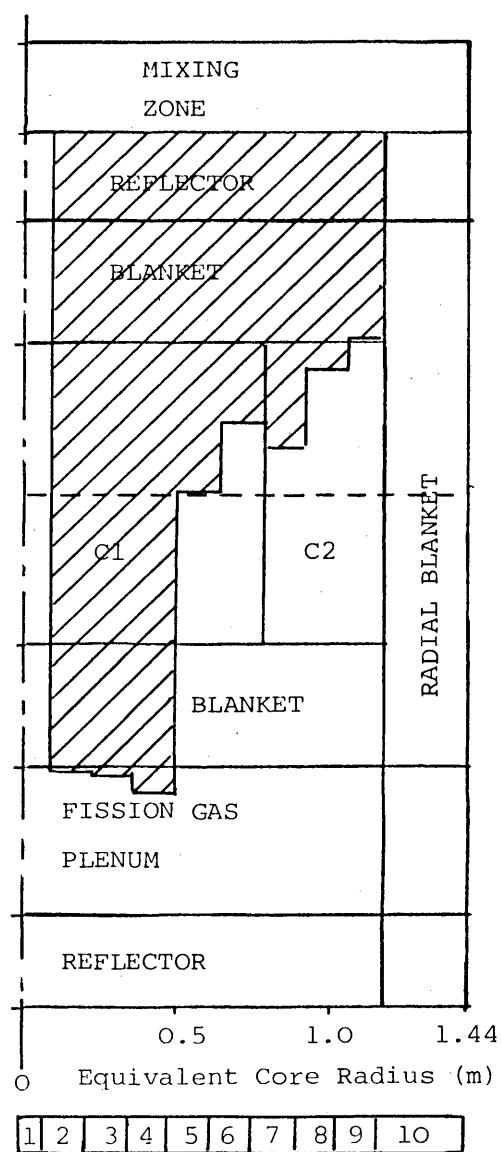


Fig. 4.9 Void-Net-and Doppler Reactivity vs. Time after
First Boiling till Onset of Disassembly (After 22,23)

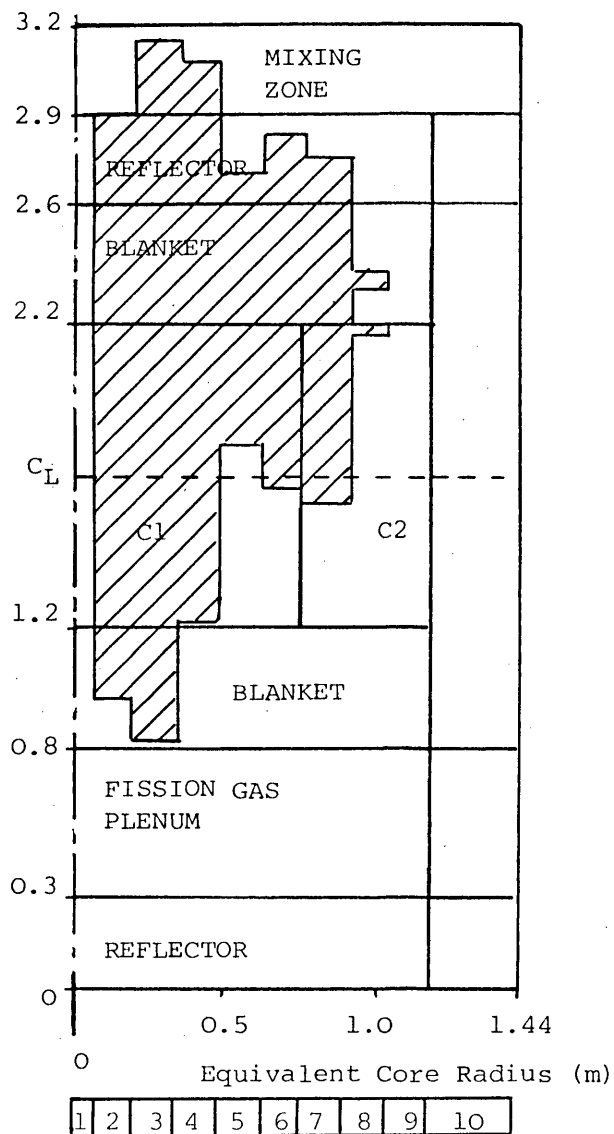


a. CAPRI-Channels

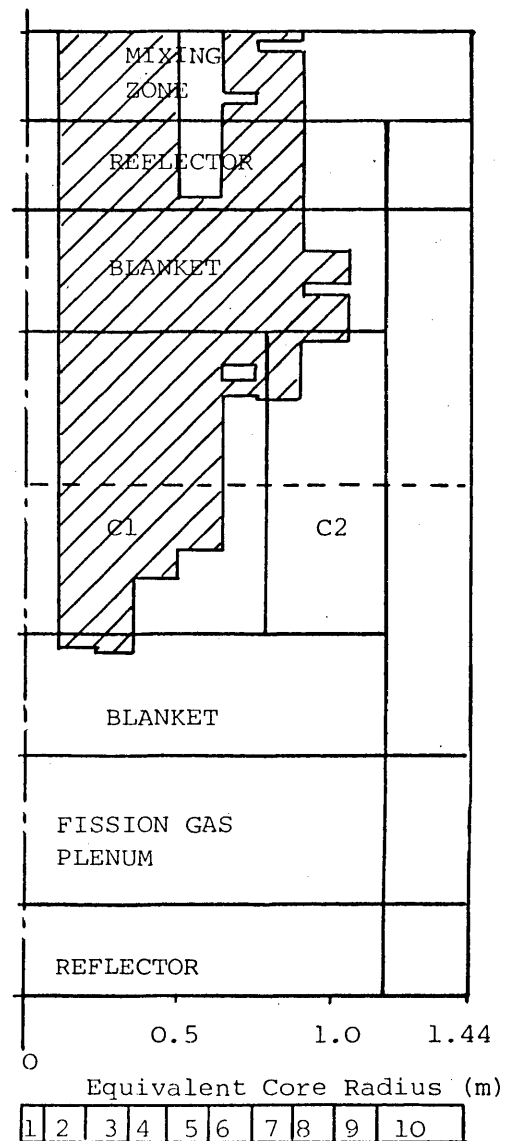


b. FRAX-Channels

Fig. 4.10 Core Voiding Patterns at Onset of Core Disassembly (After 22, 23)



c. CARMEN-Channels



d. SAS-Channels

Fig. 4.10 Core Voiding Patterns at Onset of Core Disassembly (After 22,23)

Fig. 5.1 Simplified Block Diagram of EXTRA

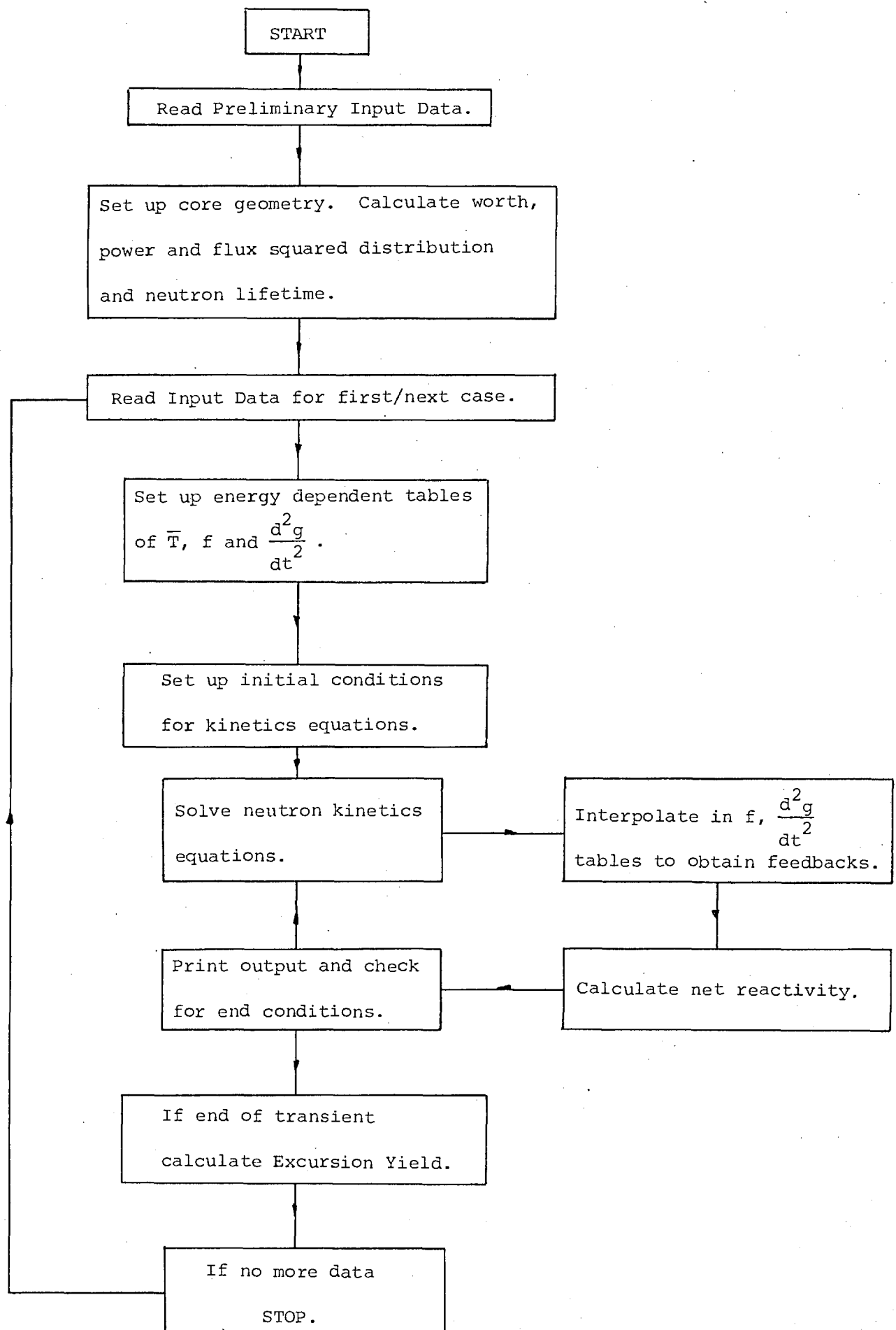


Fig. 5.2 Simplified Block Diagram of VENUS-II (after 29)

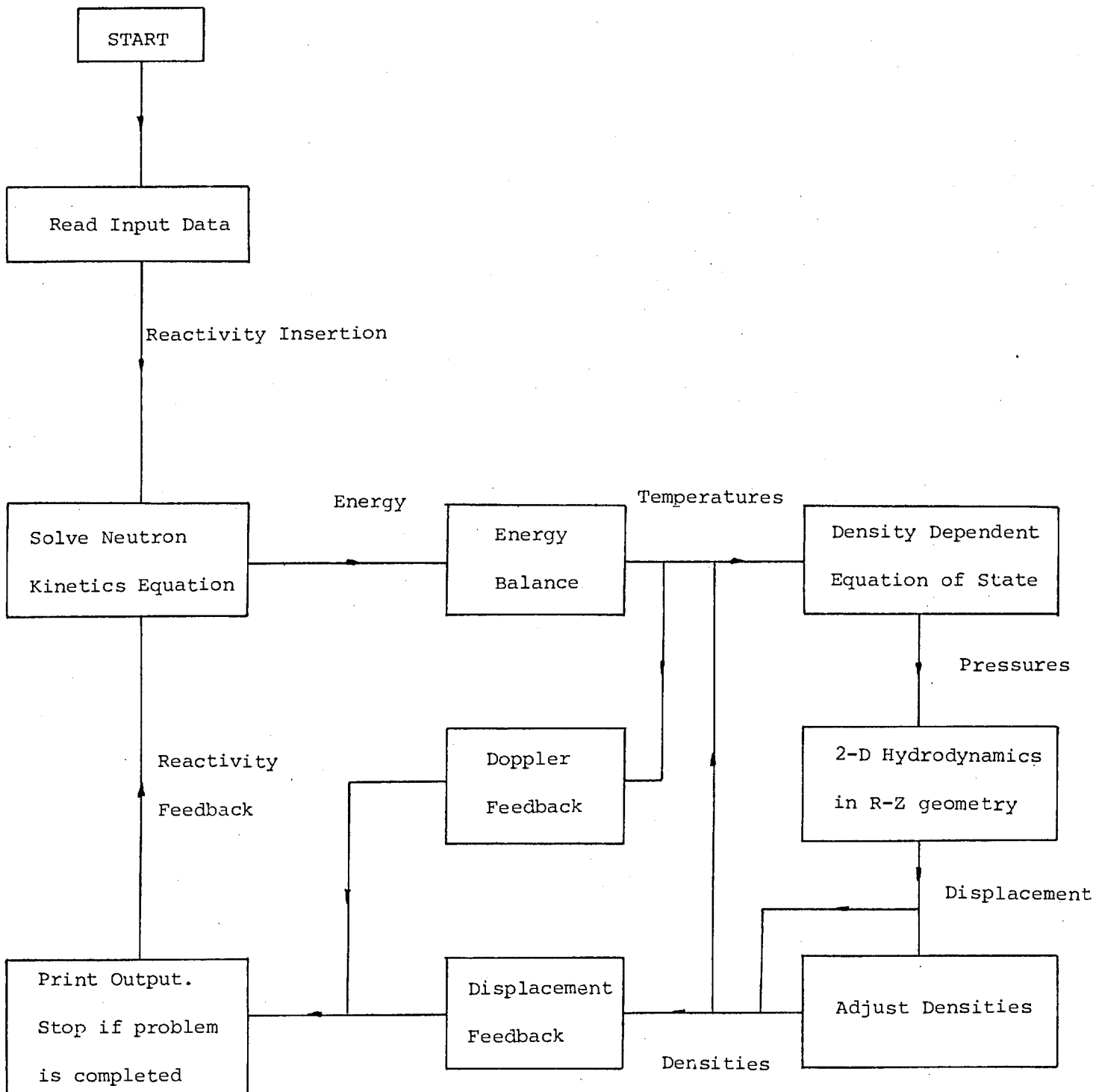


Fig. 6.1 Code Representation of Core A

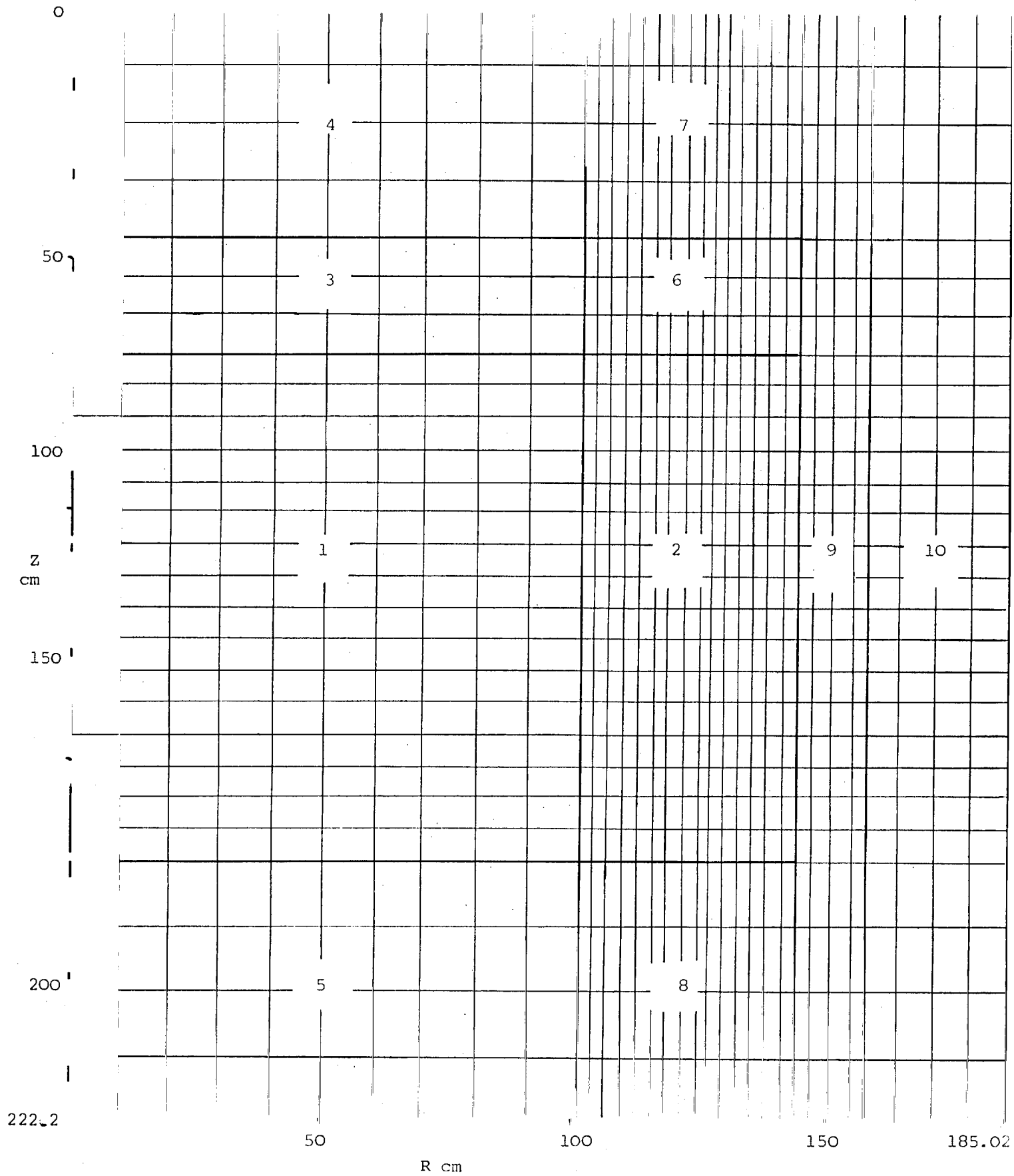


Fig. 6.2 Code Representation of Core B

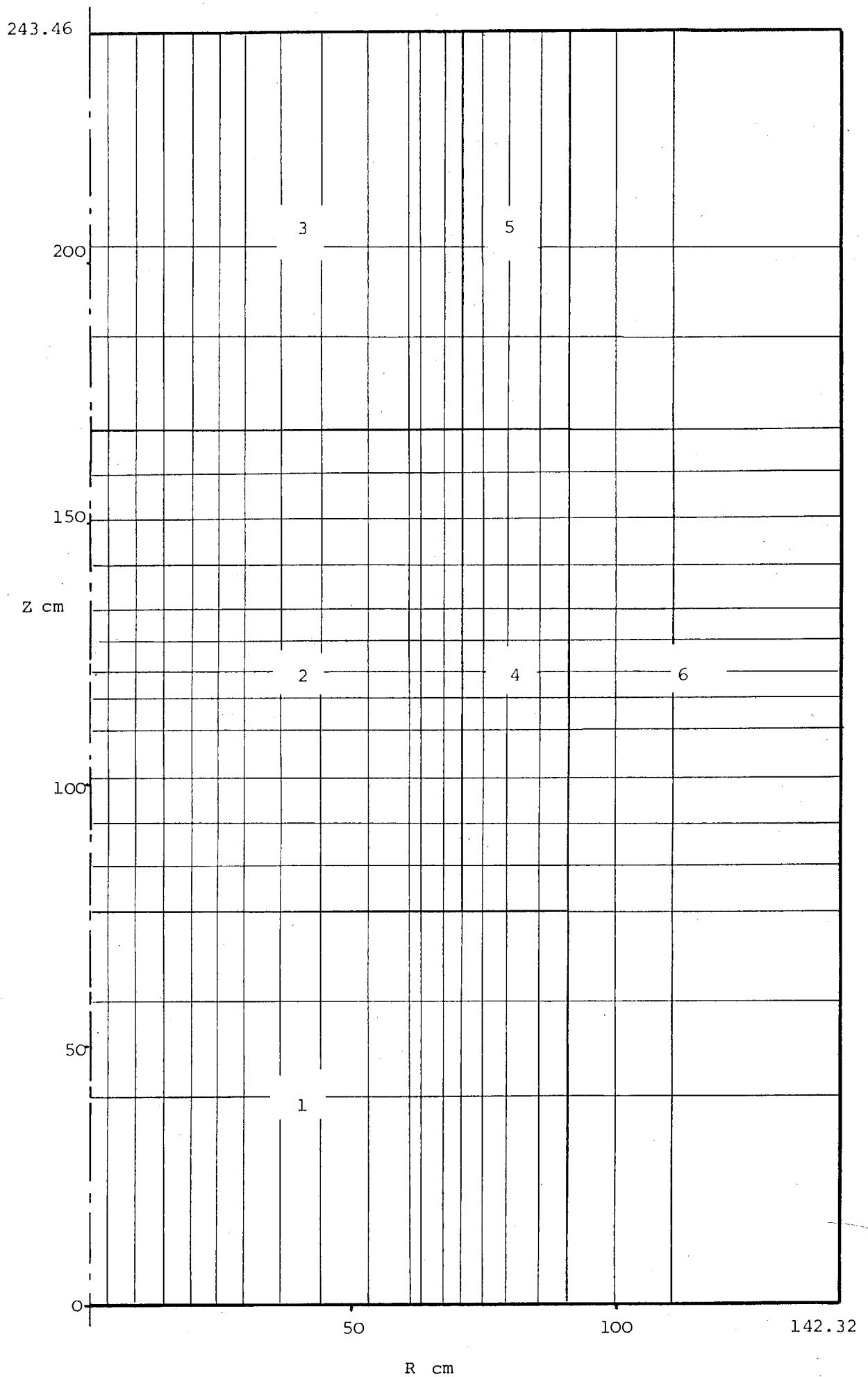


Fig. 6.3a Radial Normalised Power Distribution along
Core Mid-Plane

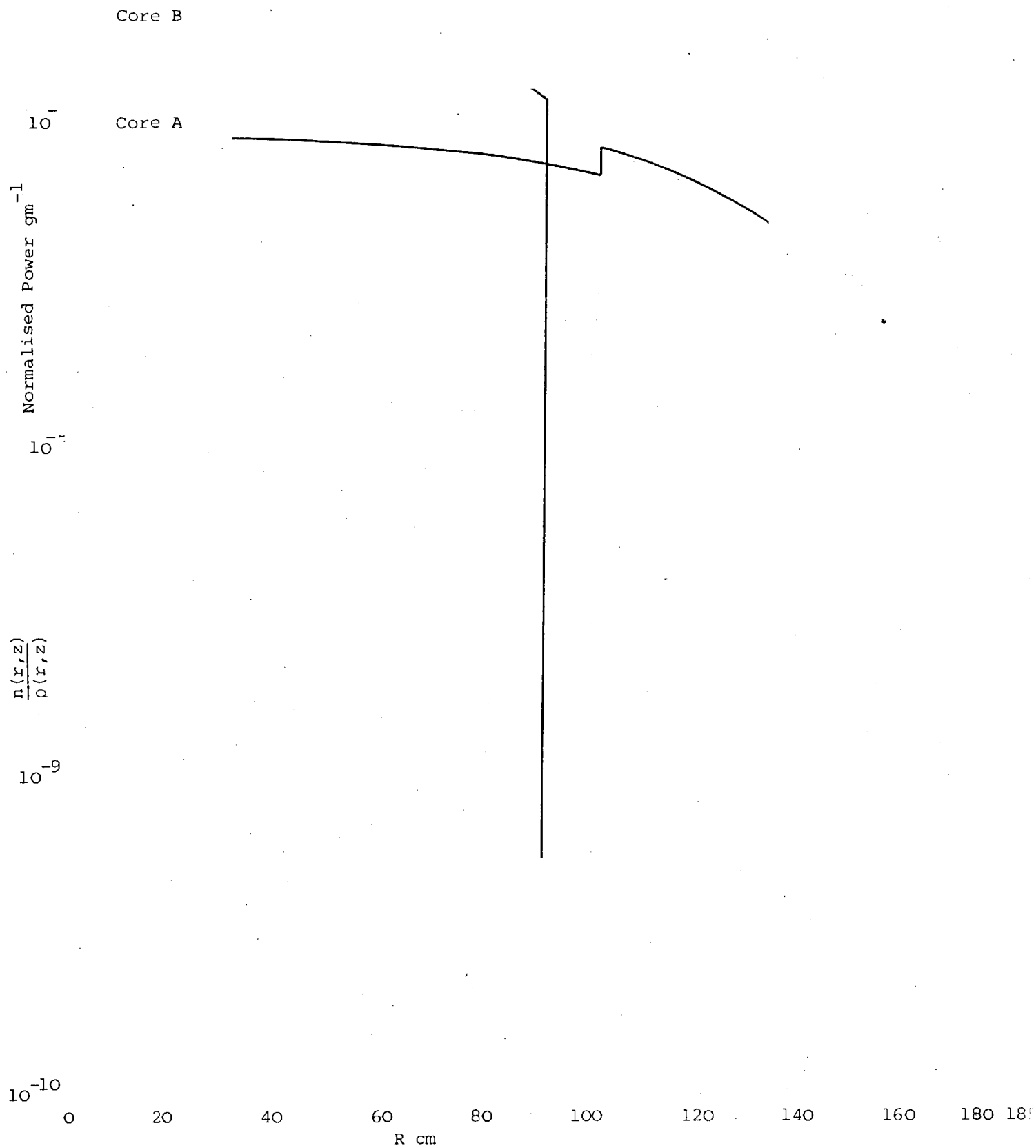


Fig. 6.3b Axial Normalised Power Distribution at Core Centre

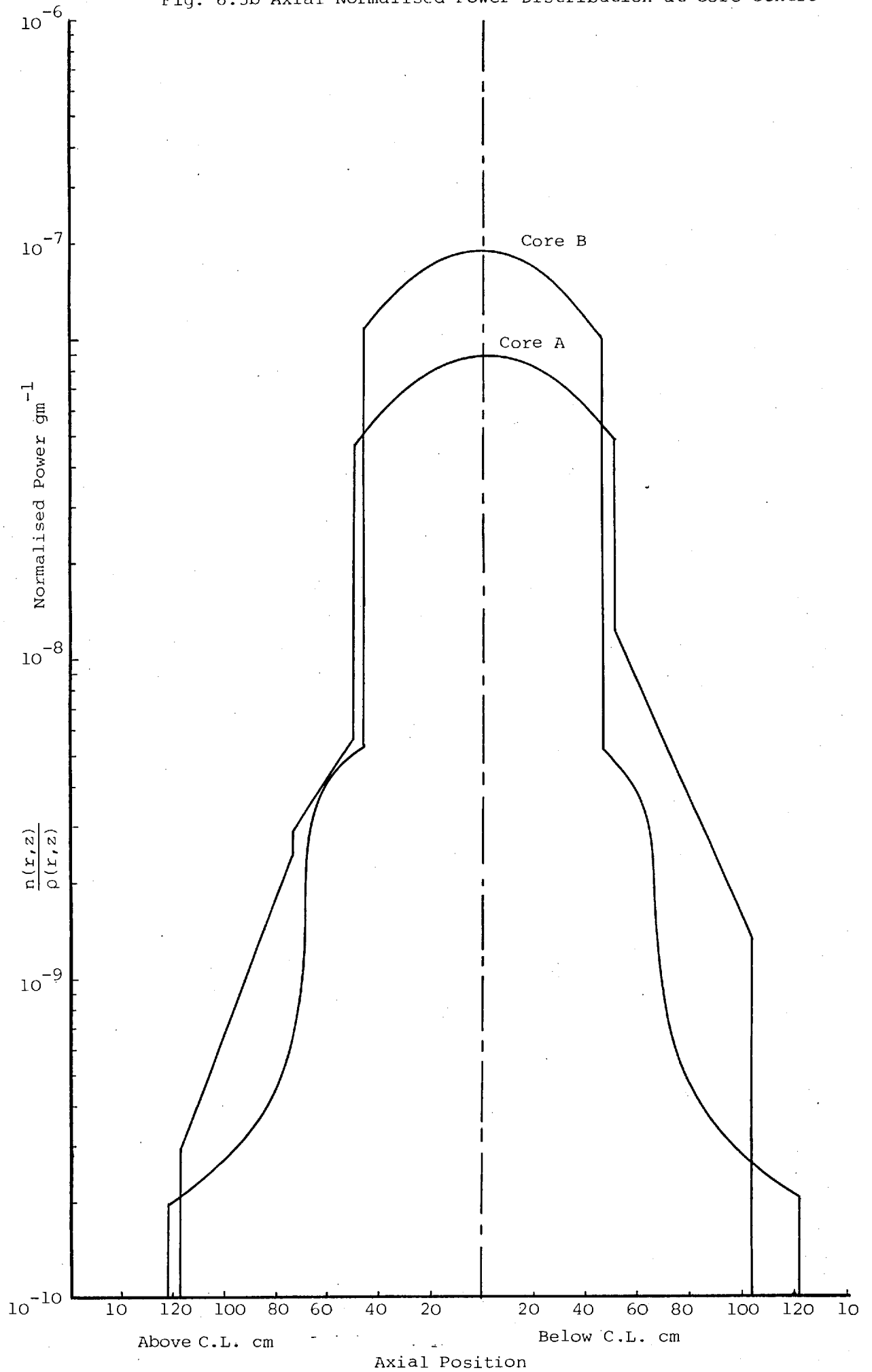


Fig. 6.4a Radial Worth Distribution Along Core Mid-Plane

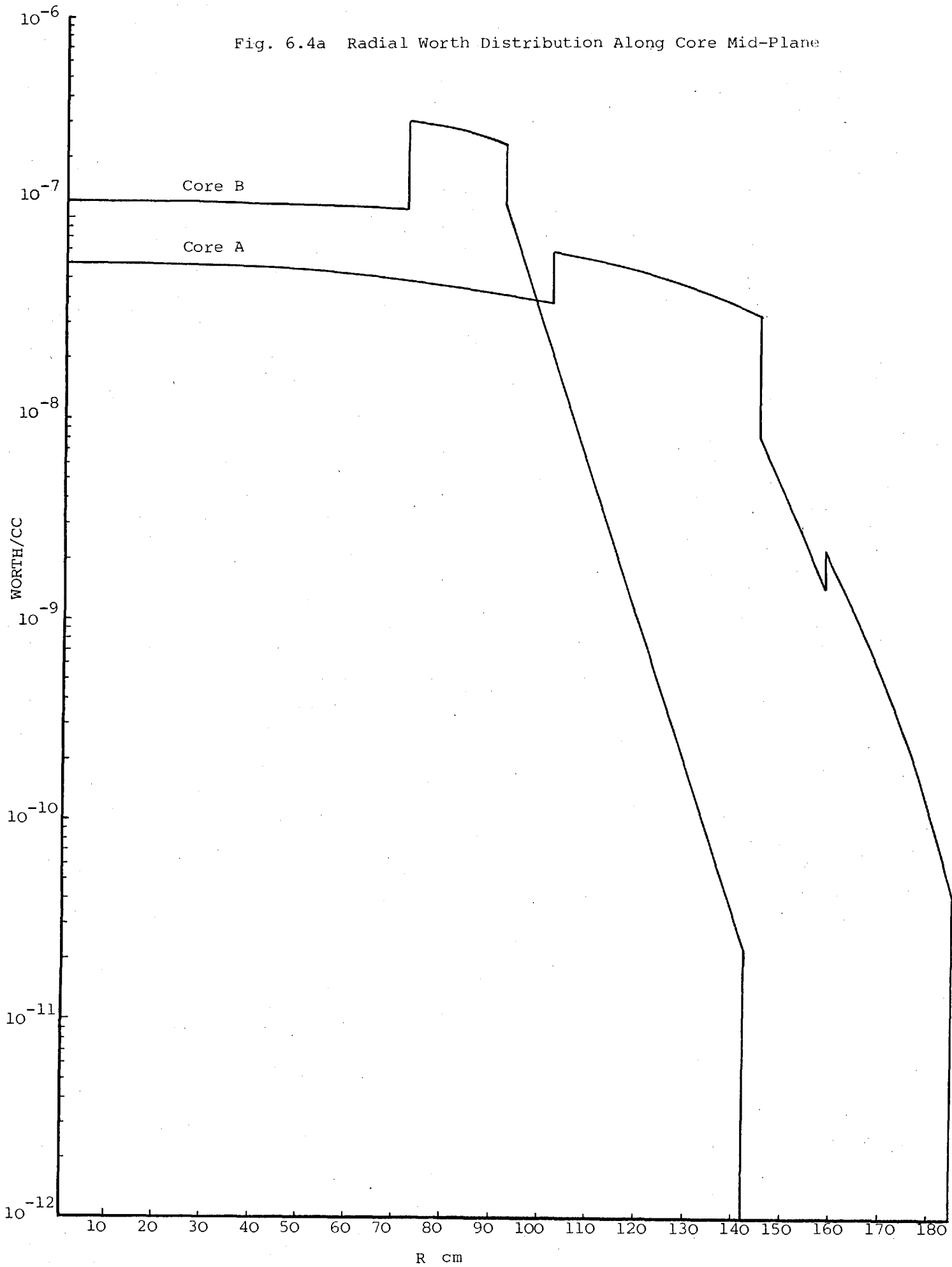


Fig. 6.4b Axial Worth Distribution at Core Centre

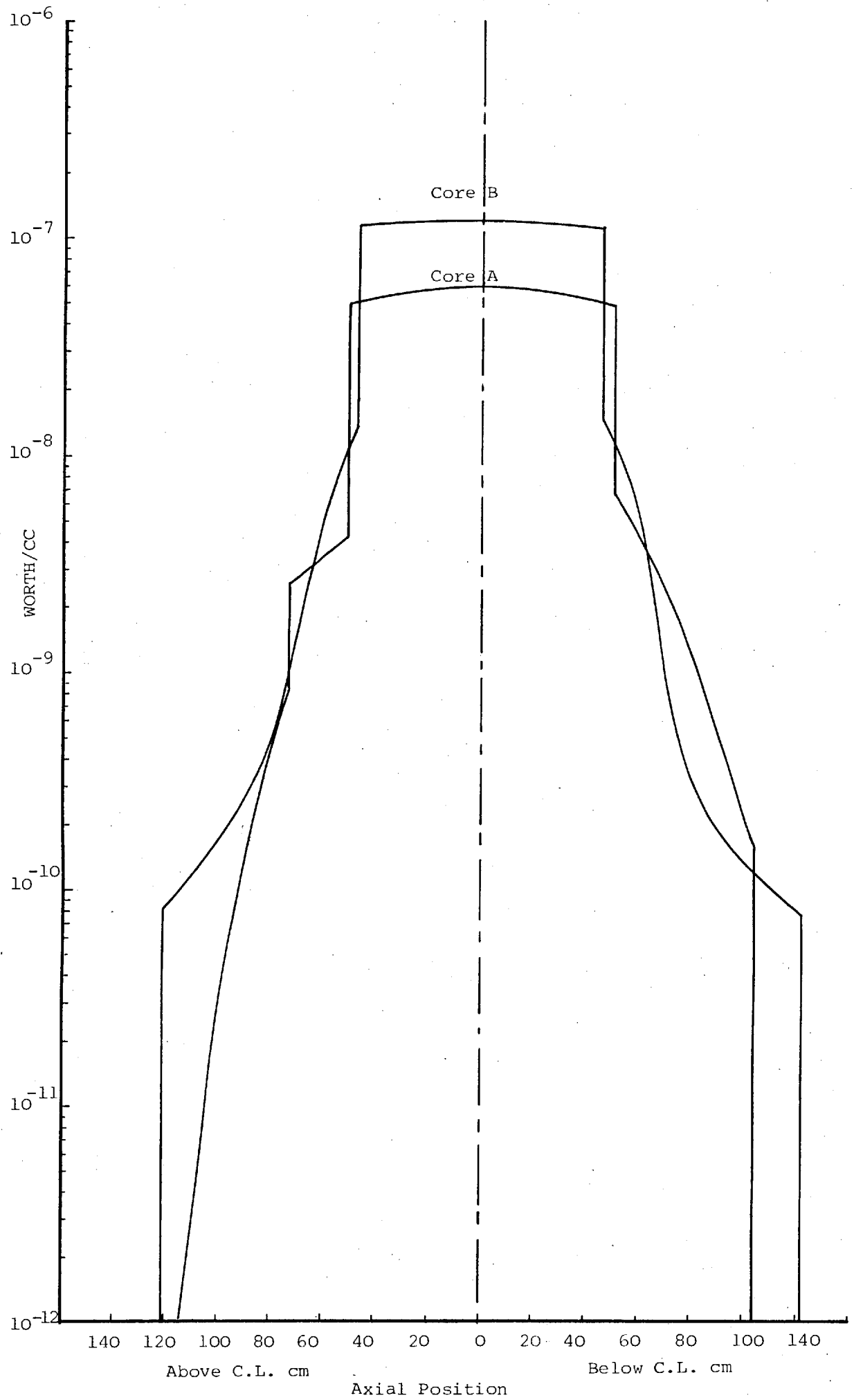
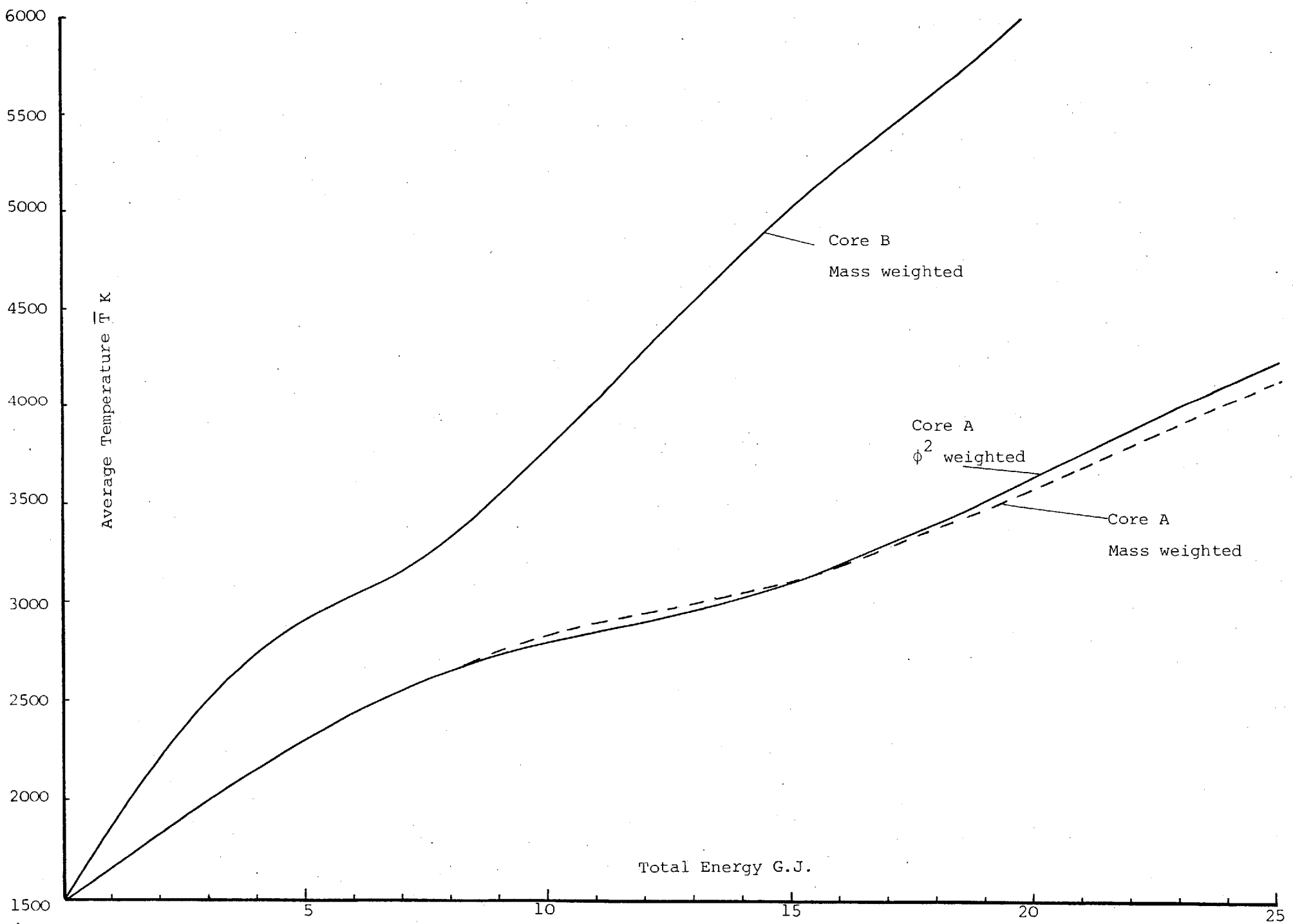


Fig. 6.5 Average Temperatures



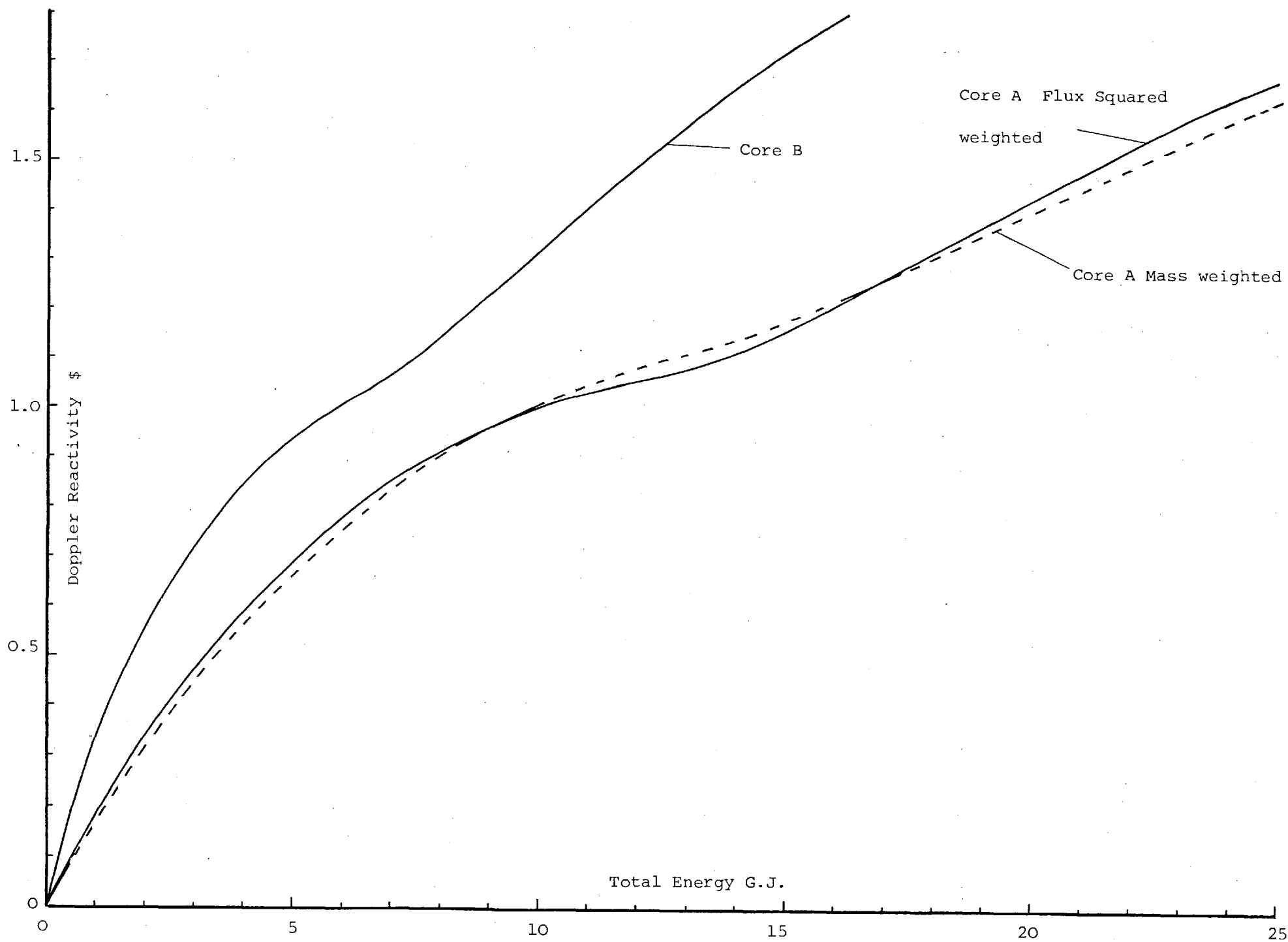


Fig. 6.6a Doppler Feedback (Doppler constant - 0.005)

Fig. 6.6b Disassembly Feedback

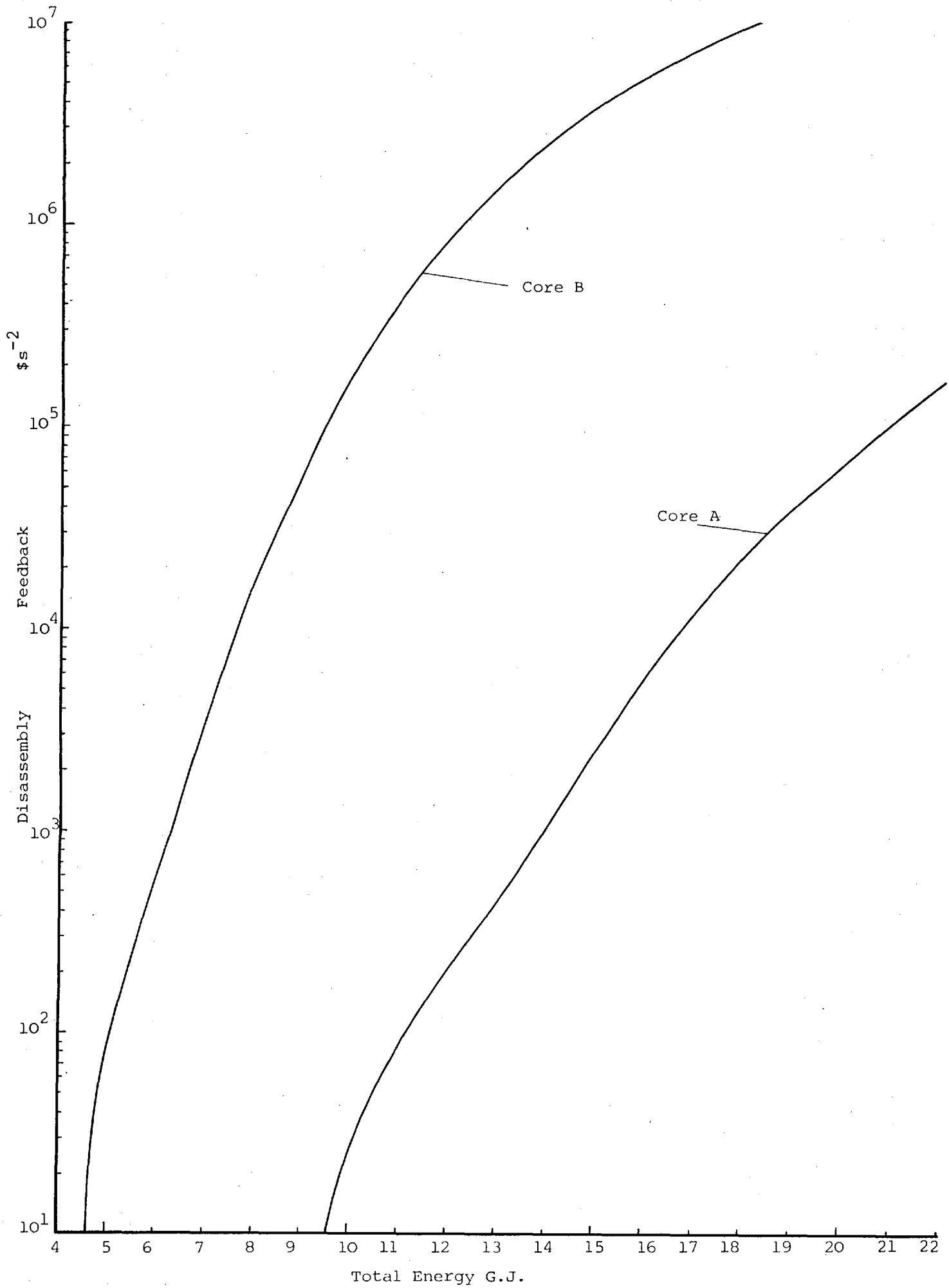
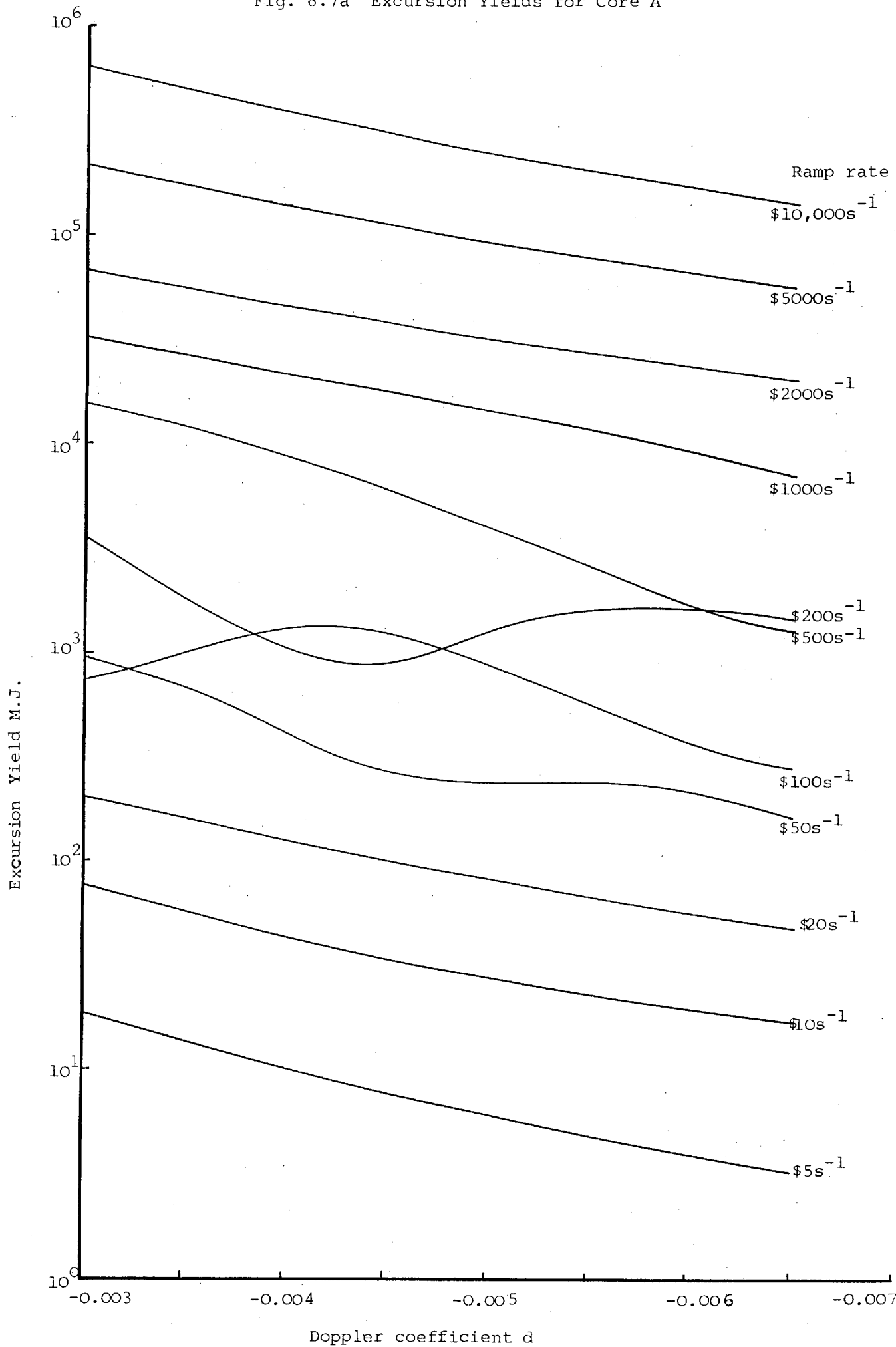
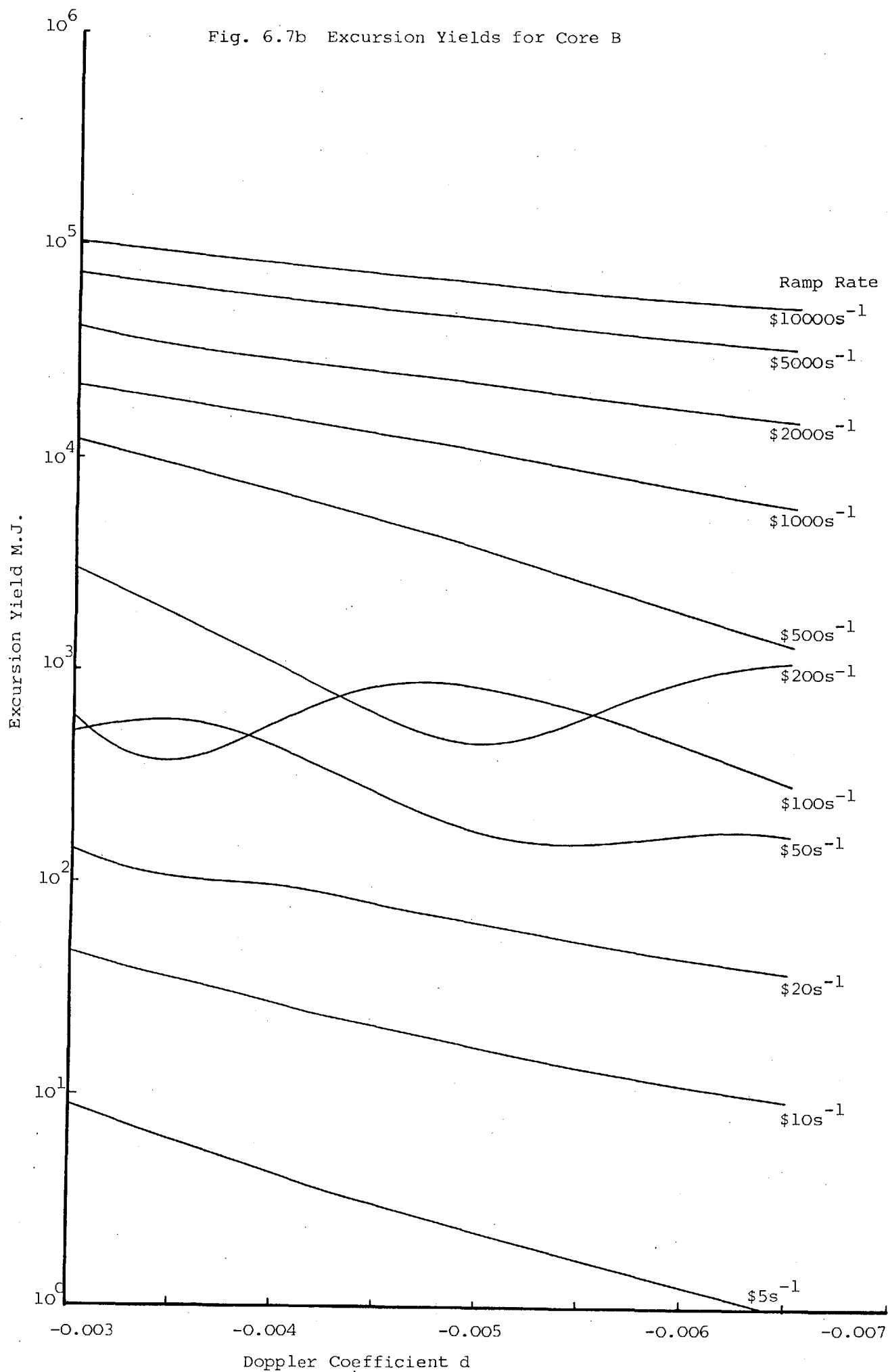


Fig. 6.7a Excursion Yields for Core A





Input Ramp Rate 100s^{-1}
Doppler Coefficient $d = -0.005$

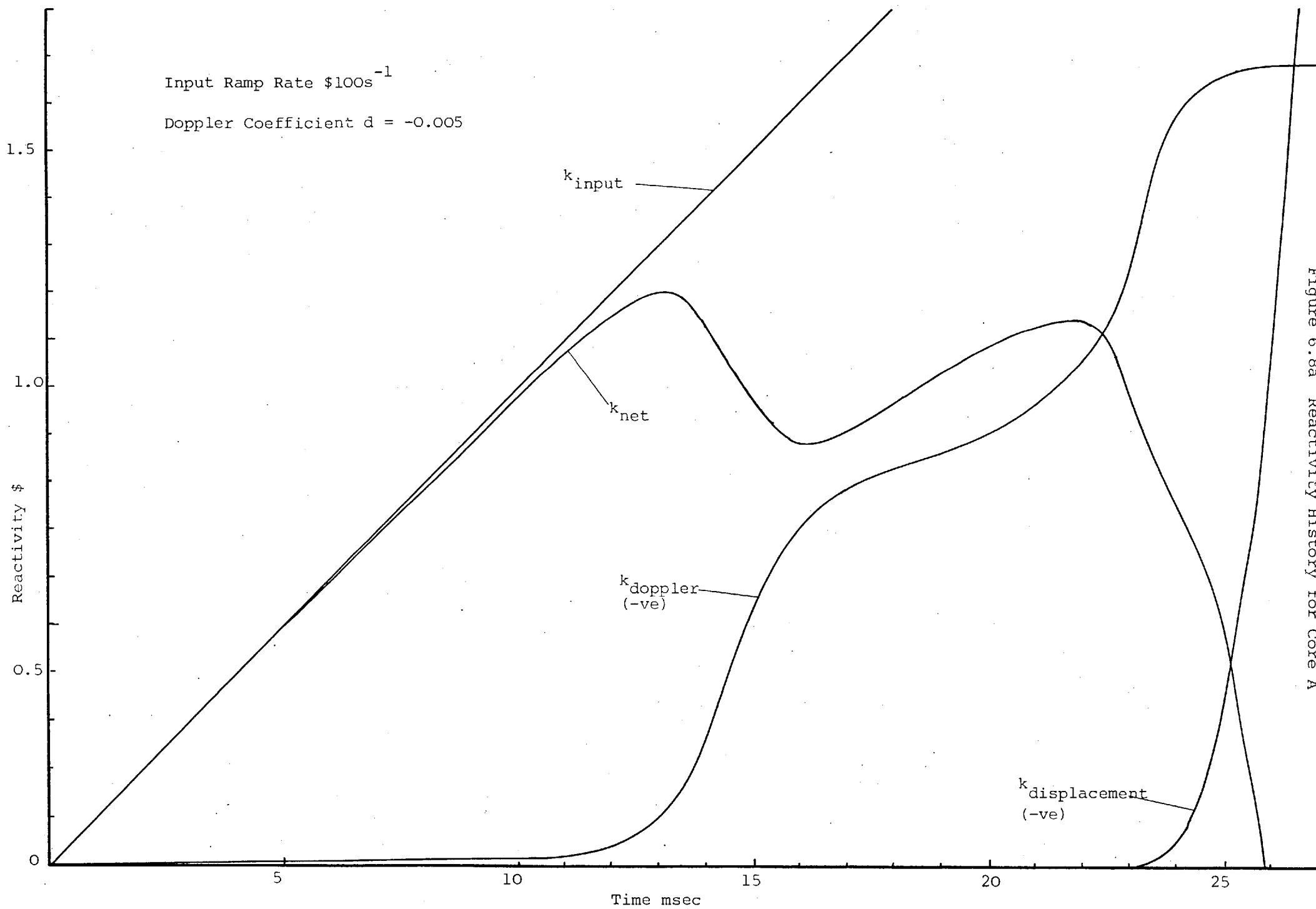


Figure 6.8a Reactivity History for Core A

Figure 6.8b Reactivity History for Core B

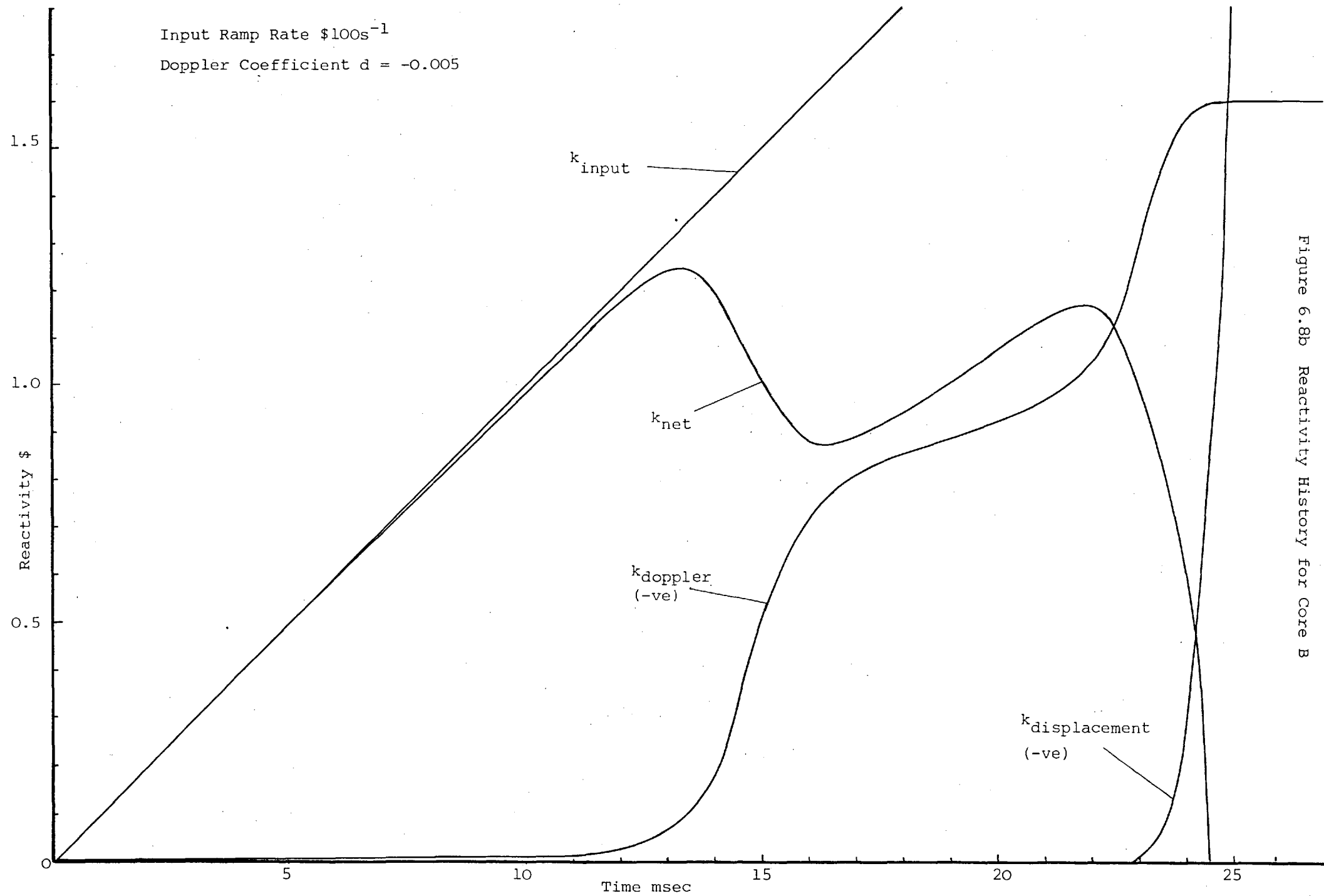
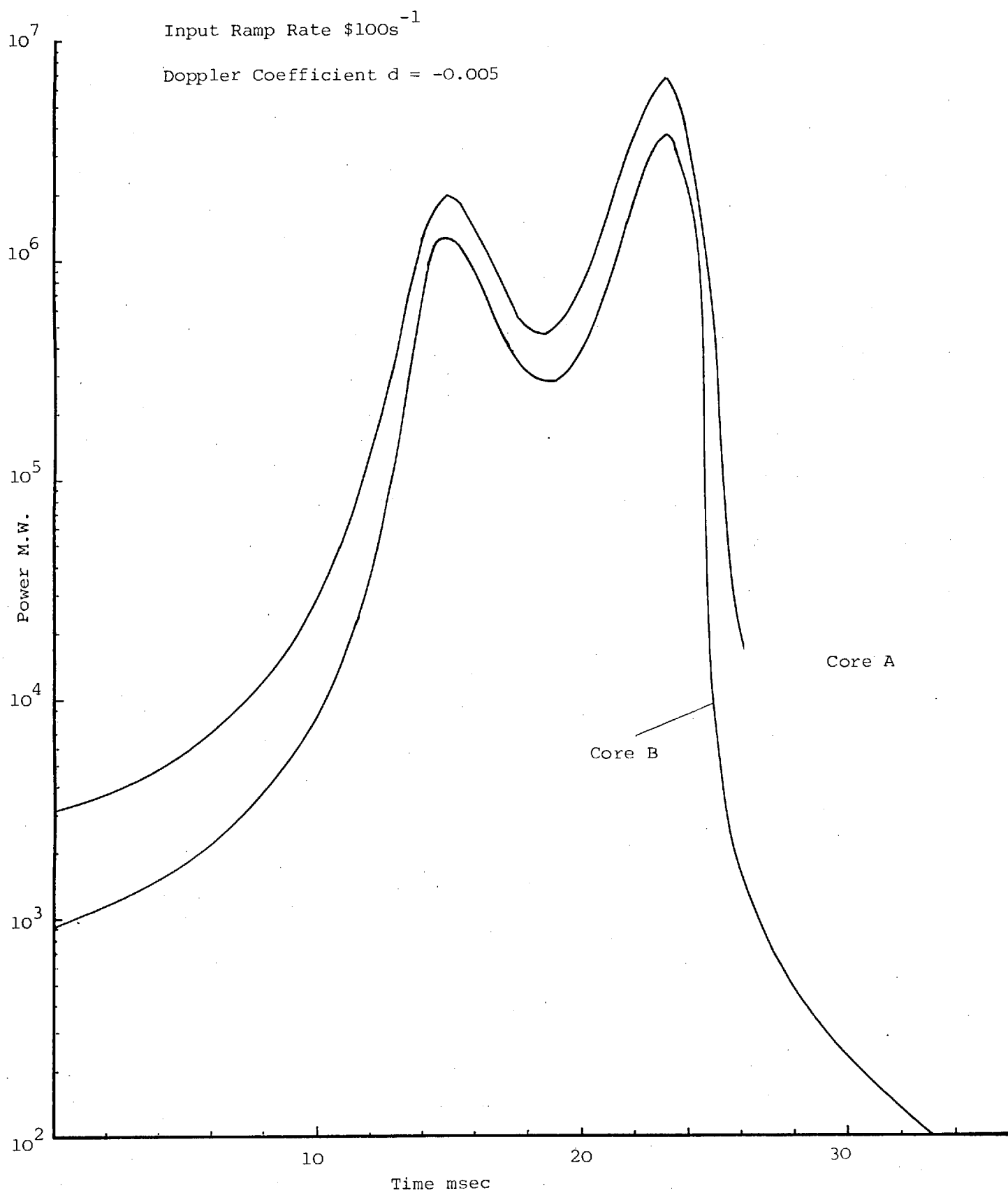


Figure 8.c Power Histories for Cores A and B



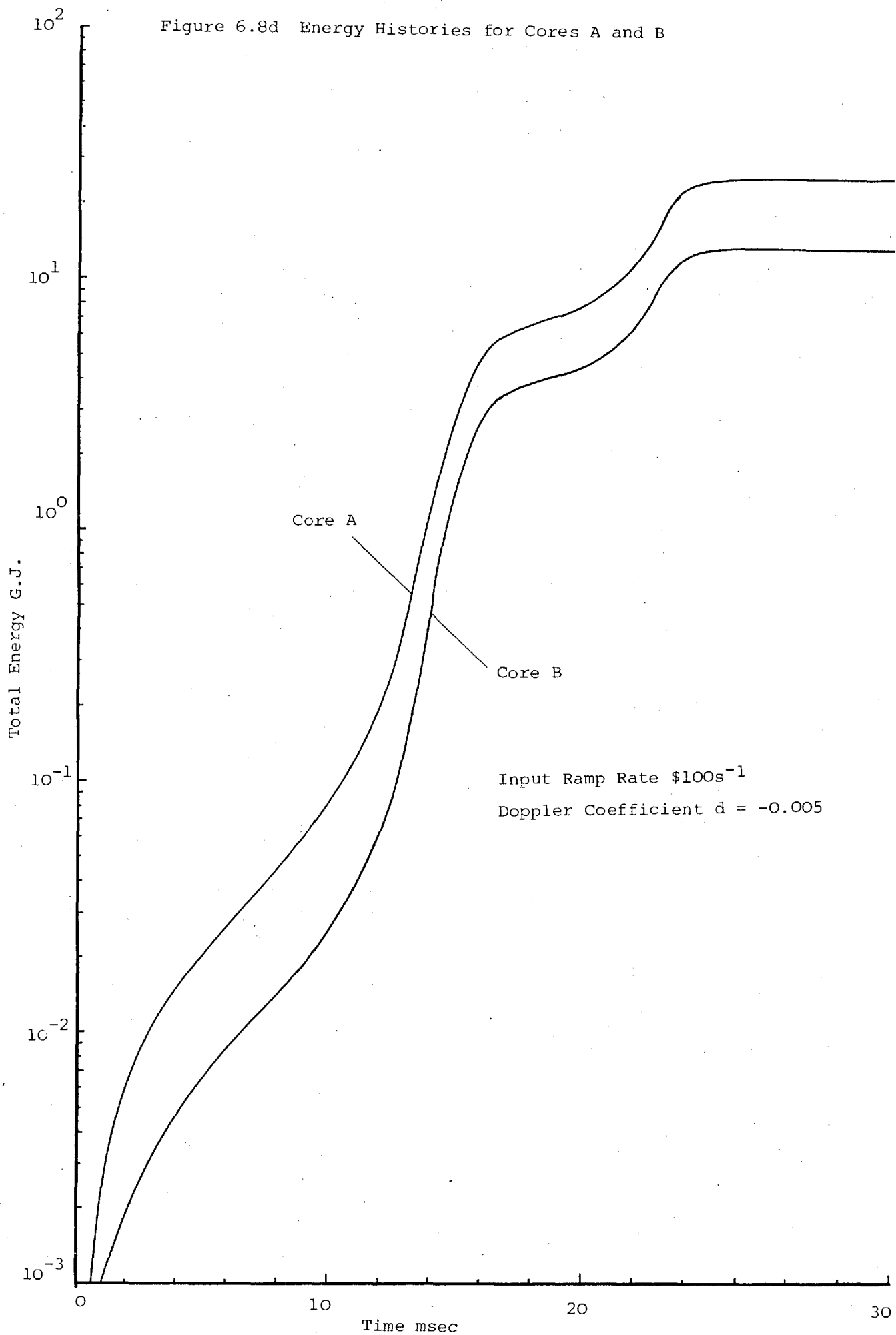


Figure 6.9 Thermal Yield vs. Excursion Yield

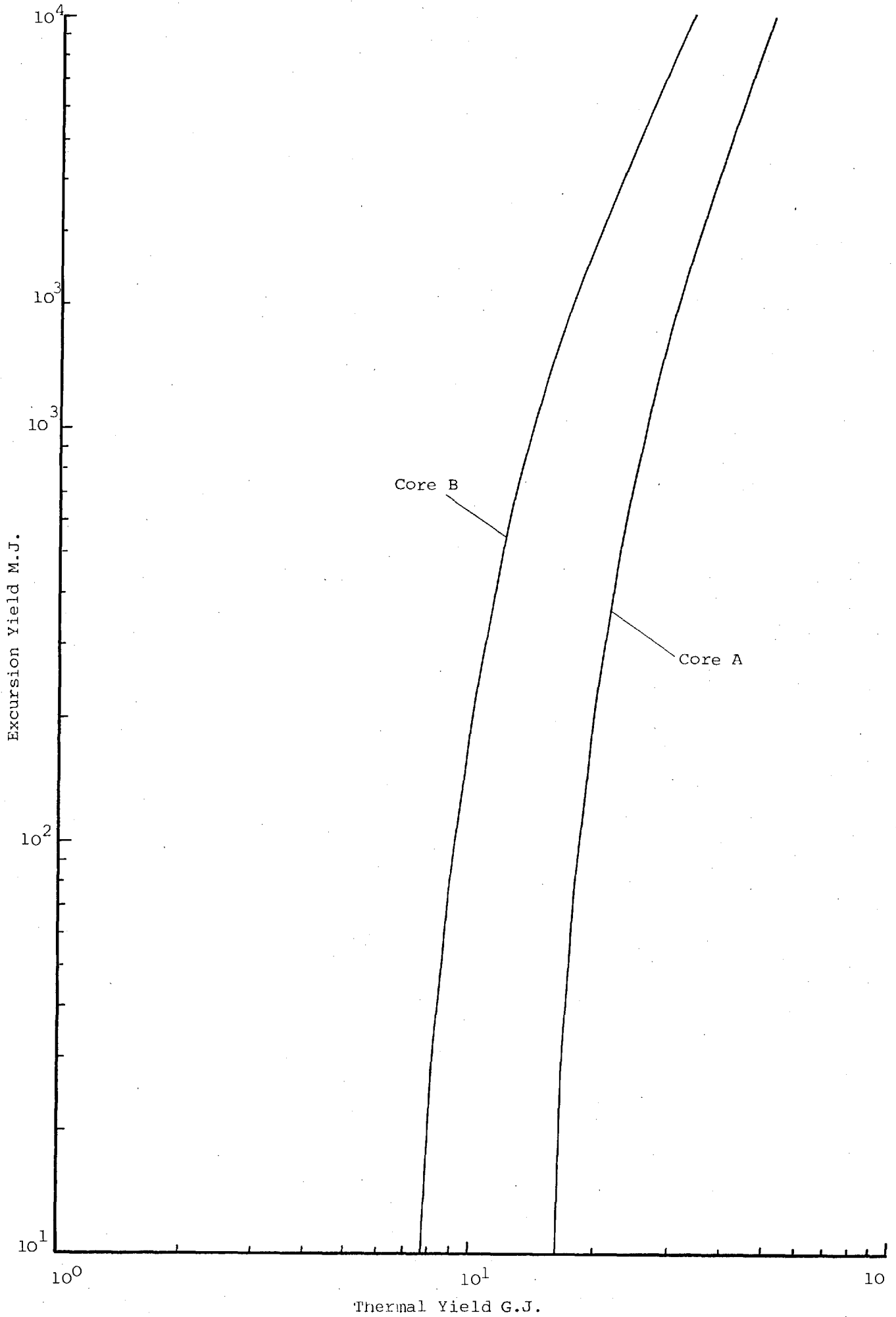


Figure 6.10 Specific Excursion Yields for Cores A and B

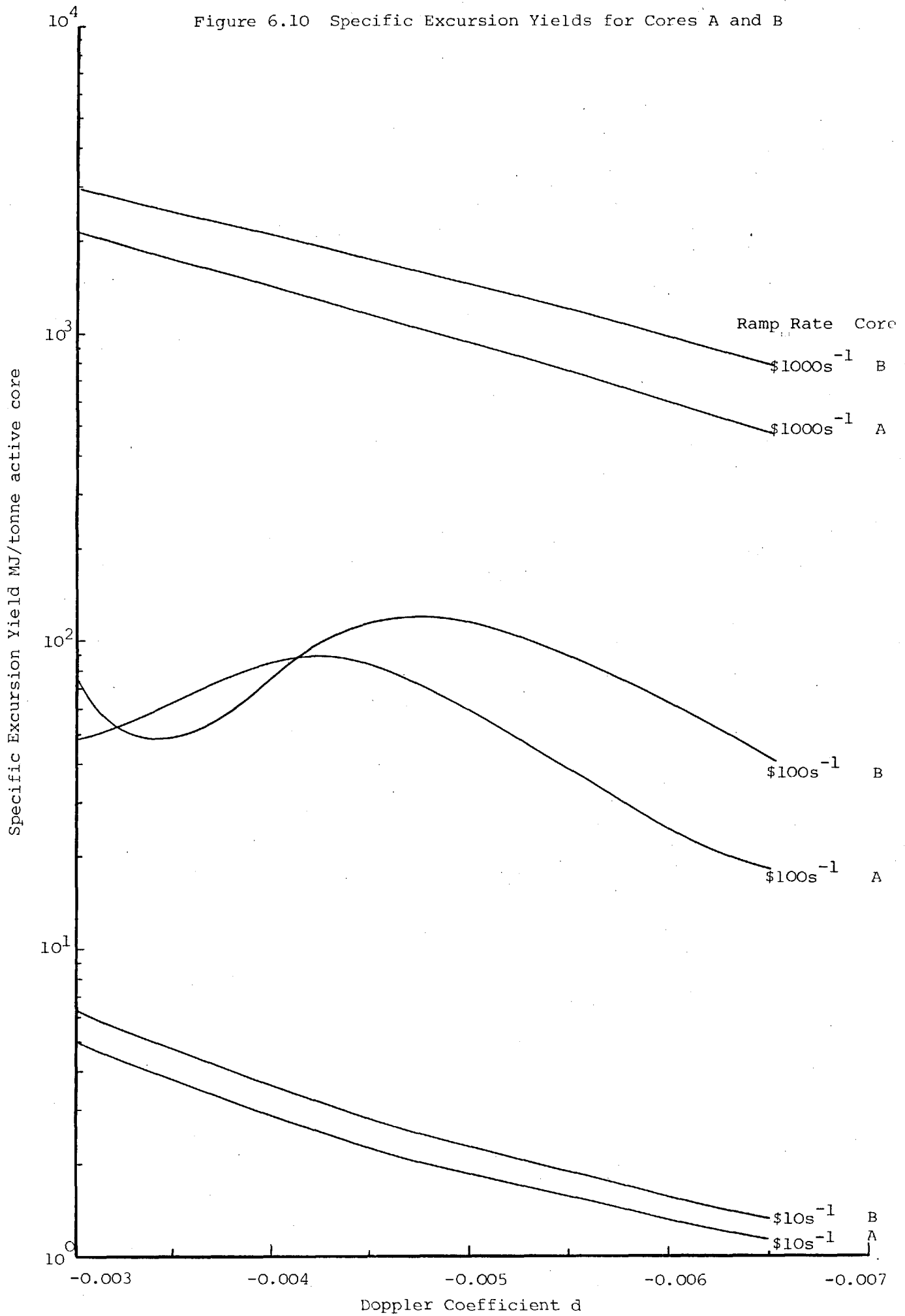


Fig. 6.11a Relative Power Histories for Cores A and B

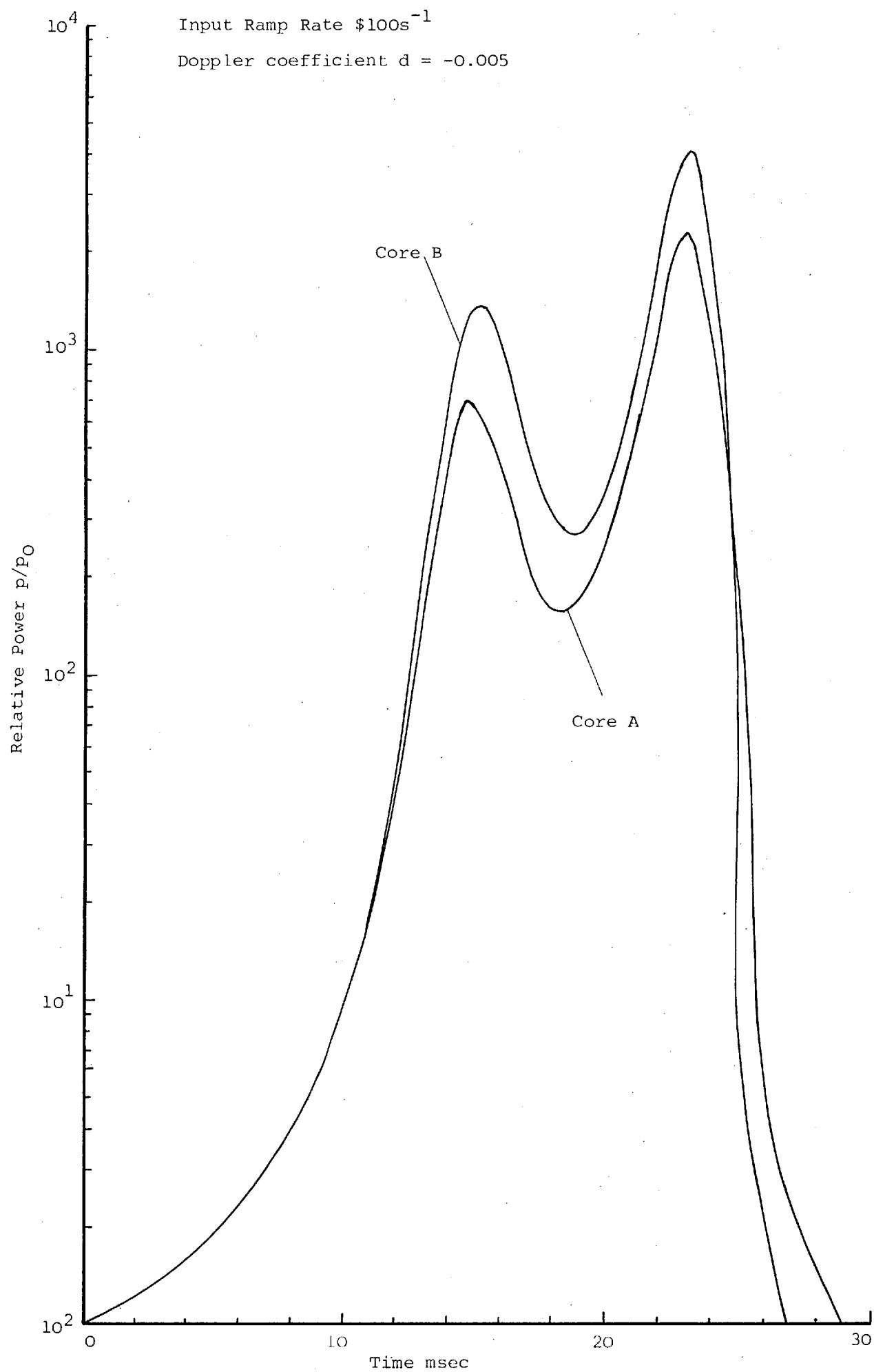


Figure 6.11b Relative Energy Deposition History

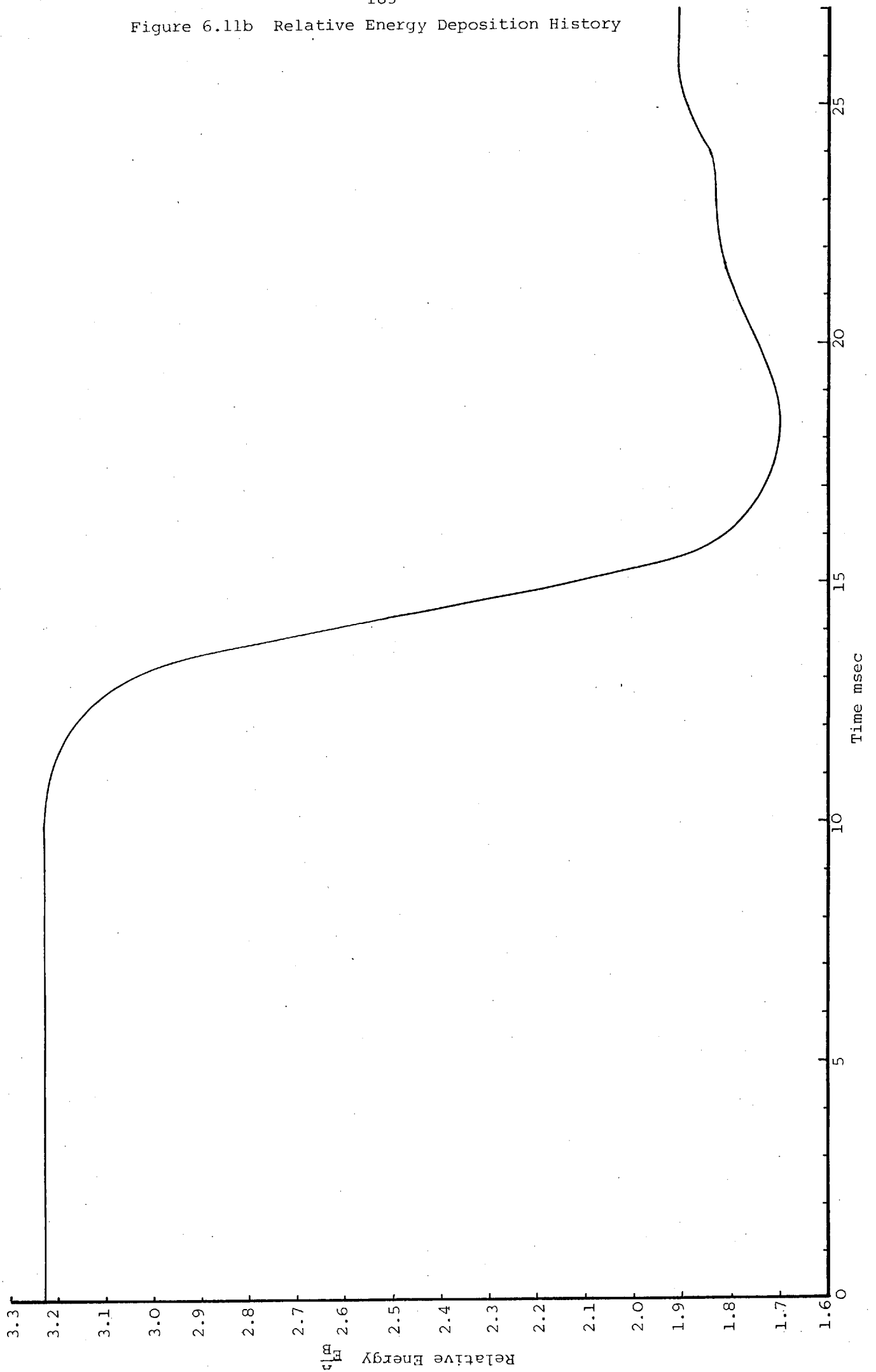


Figure 6.12a Variation of Temperature with Time - Core A

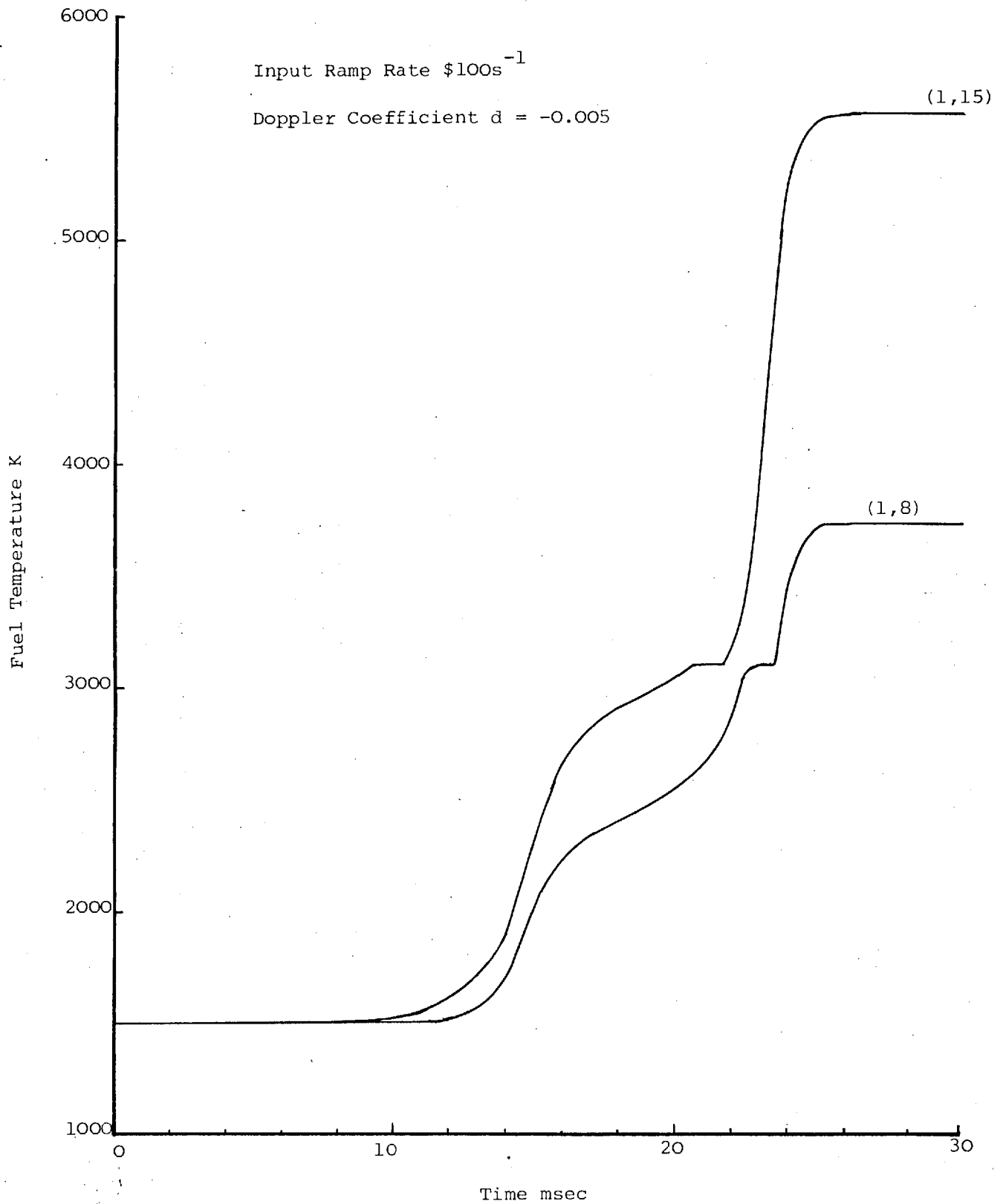


Figure 6.12b Radial Distribution of Temperature - Core A

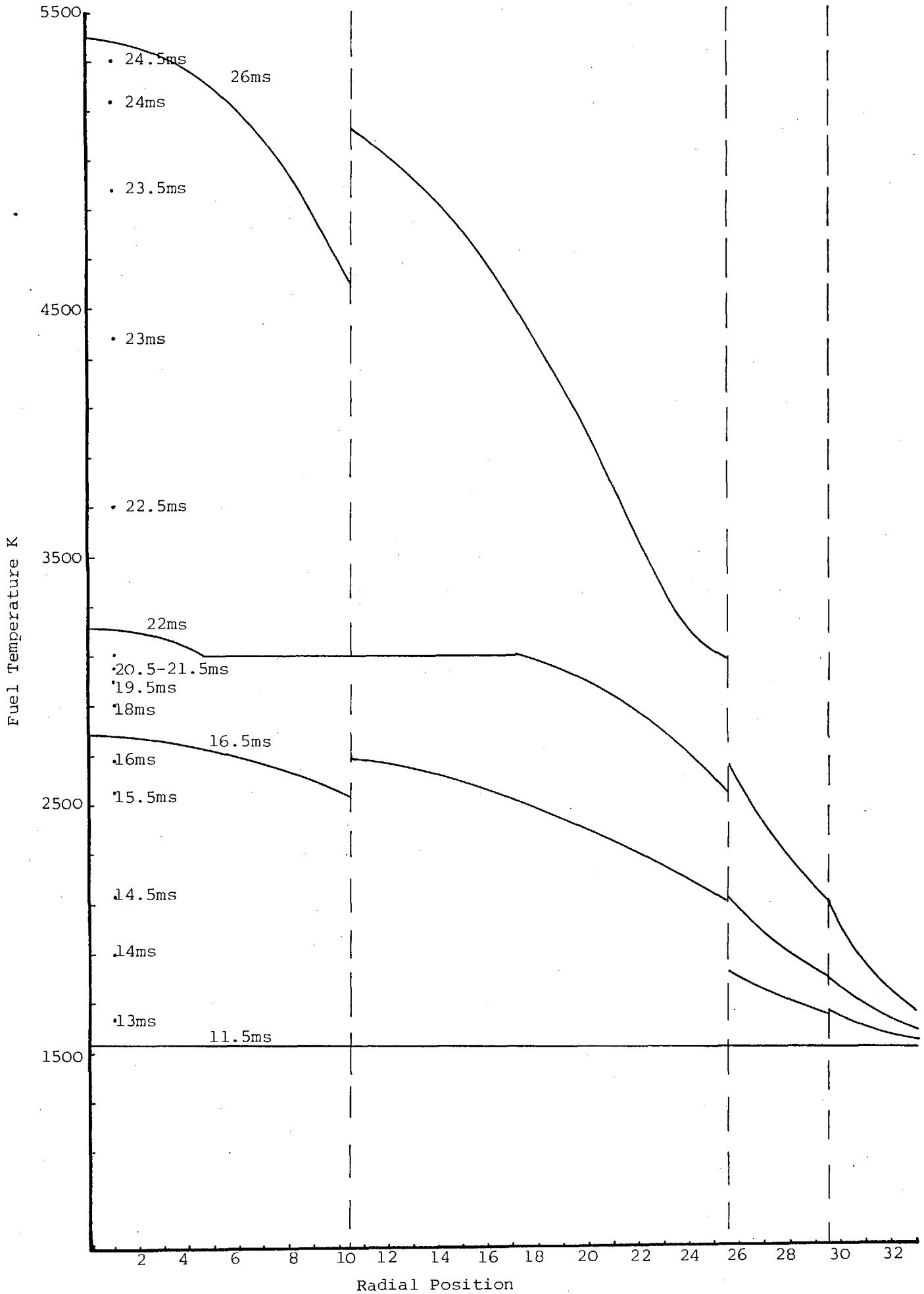


Fig. 6.12c Axial Distribution of Temperature - Core A

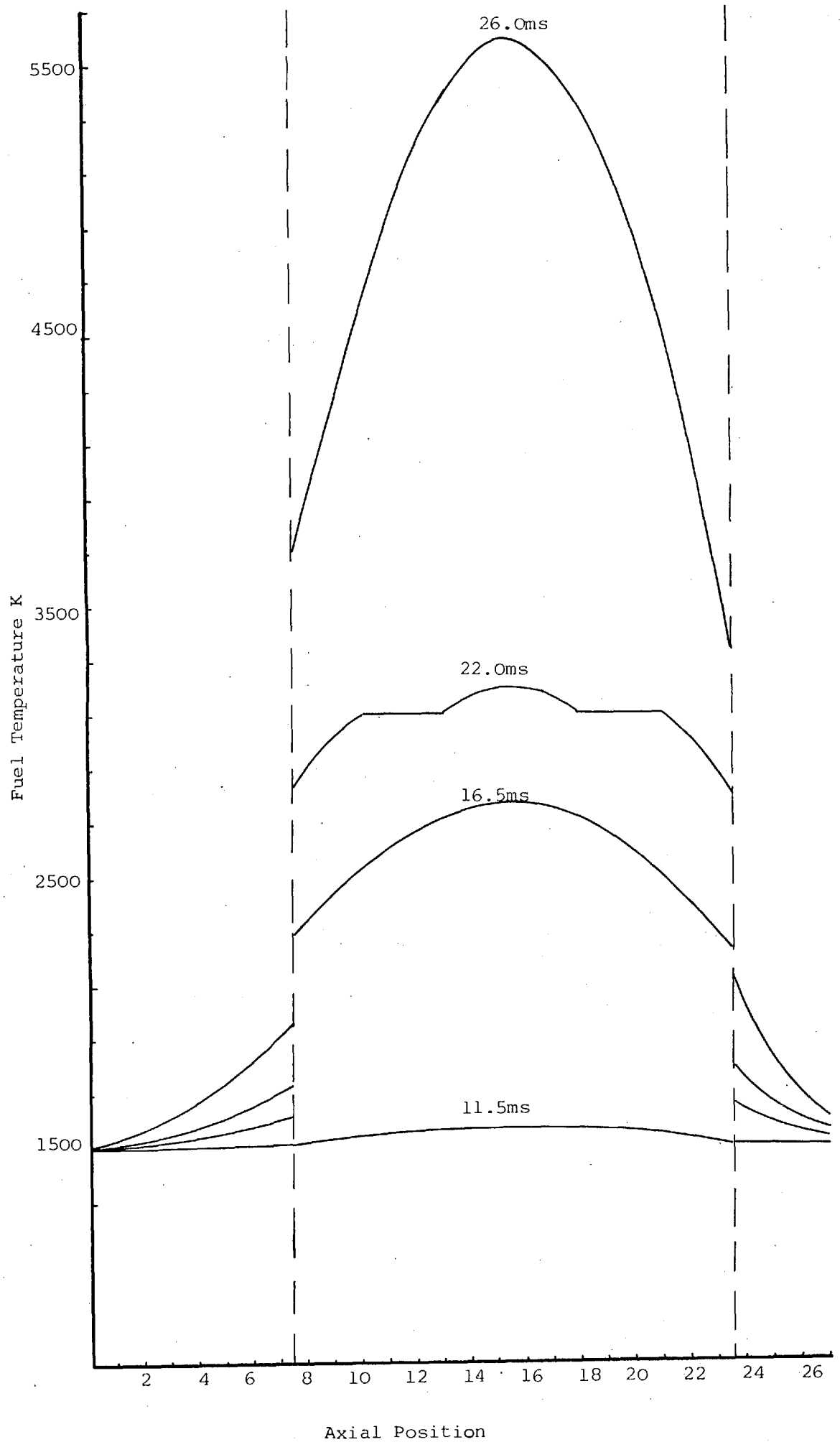


Figure 6.13a Build up of Pressure with Time -

Core A

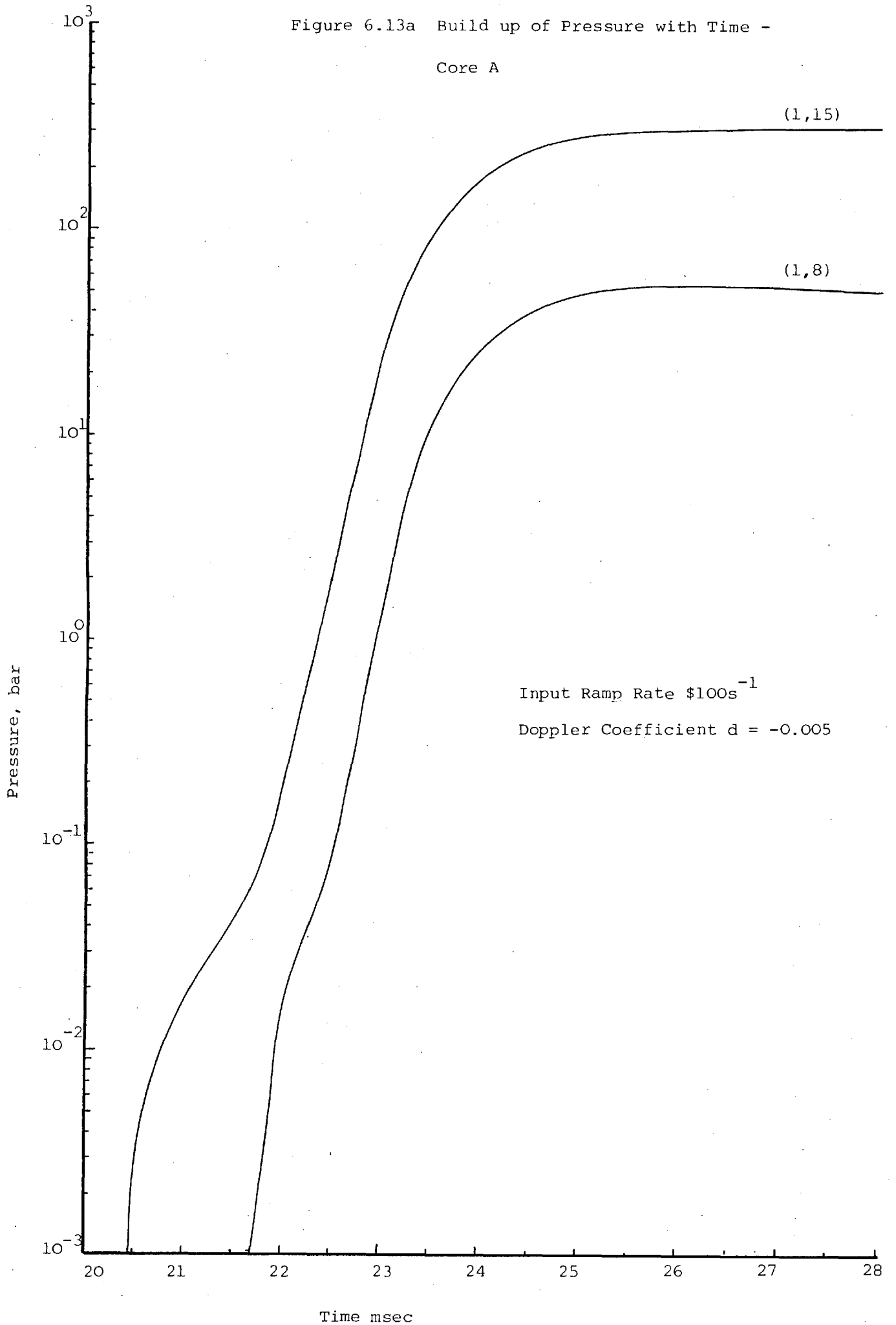


Figure 6.13b Radial Pressure Distribution

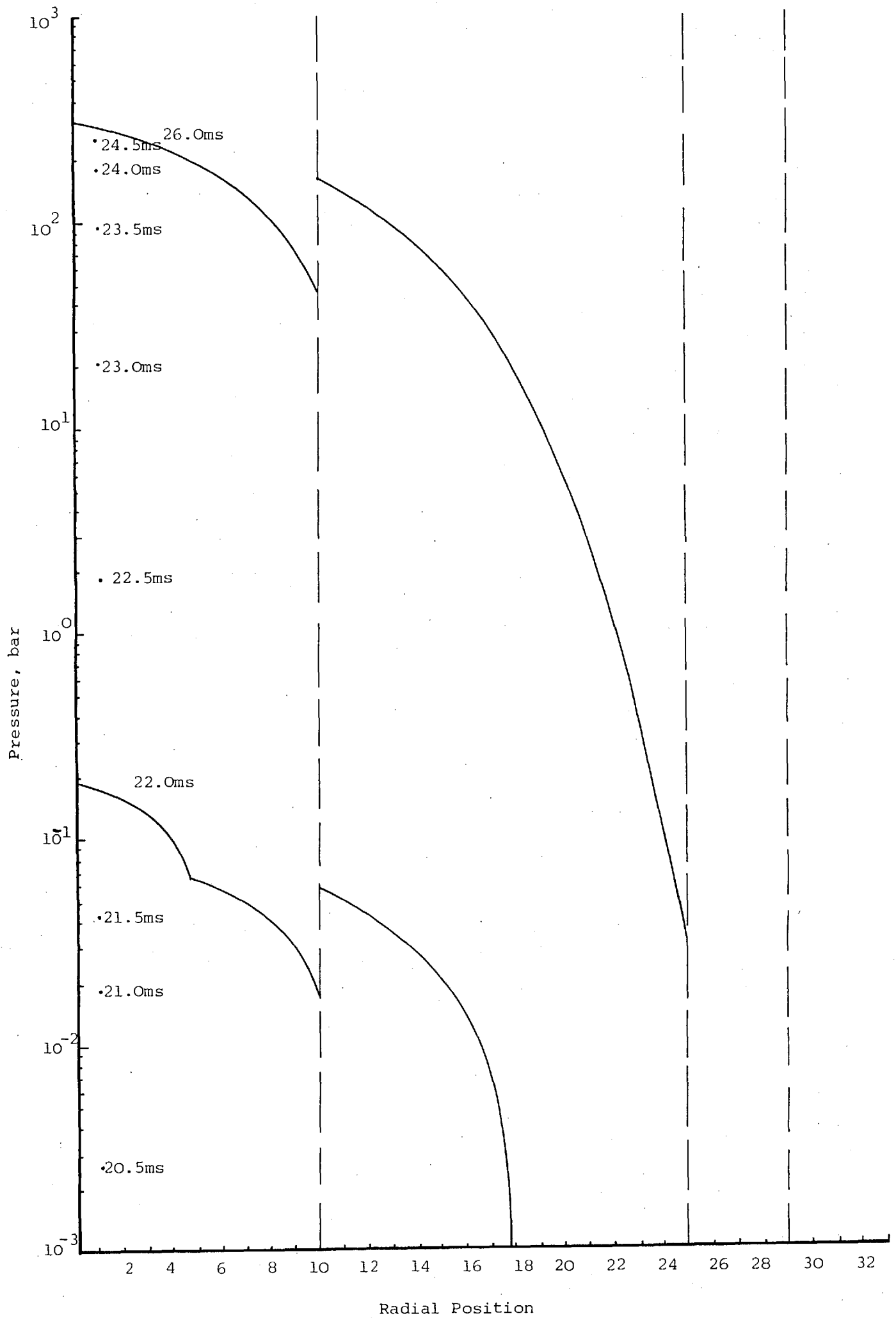


Figure 6.13c Axial Pressure Distribution - Core A

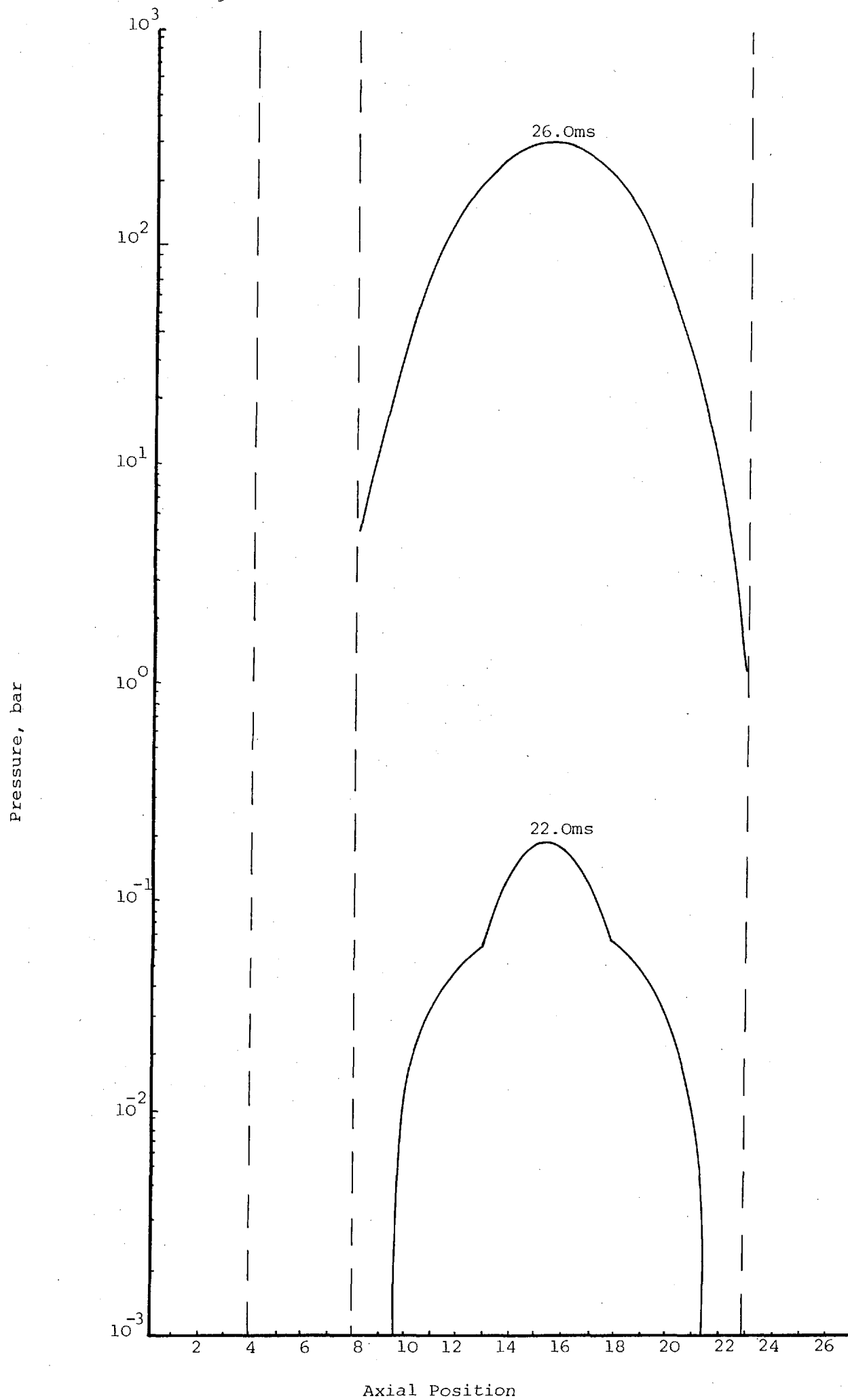


Figure 6.14a Final Pressure Distribution - Core A

1. G.J. Yield

Pressure in bar

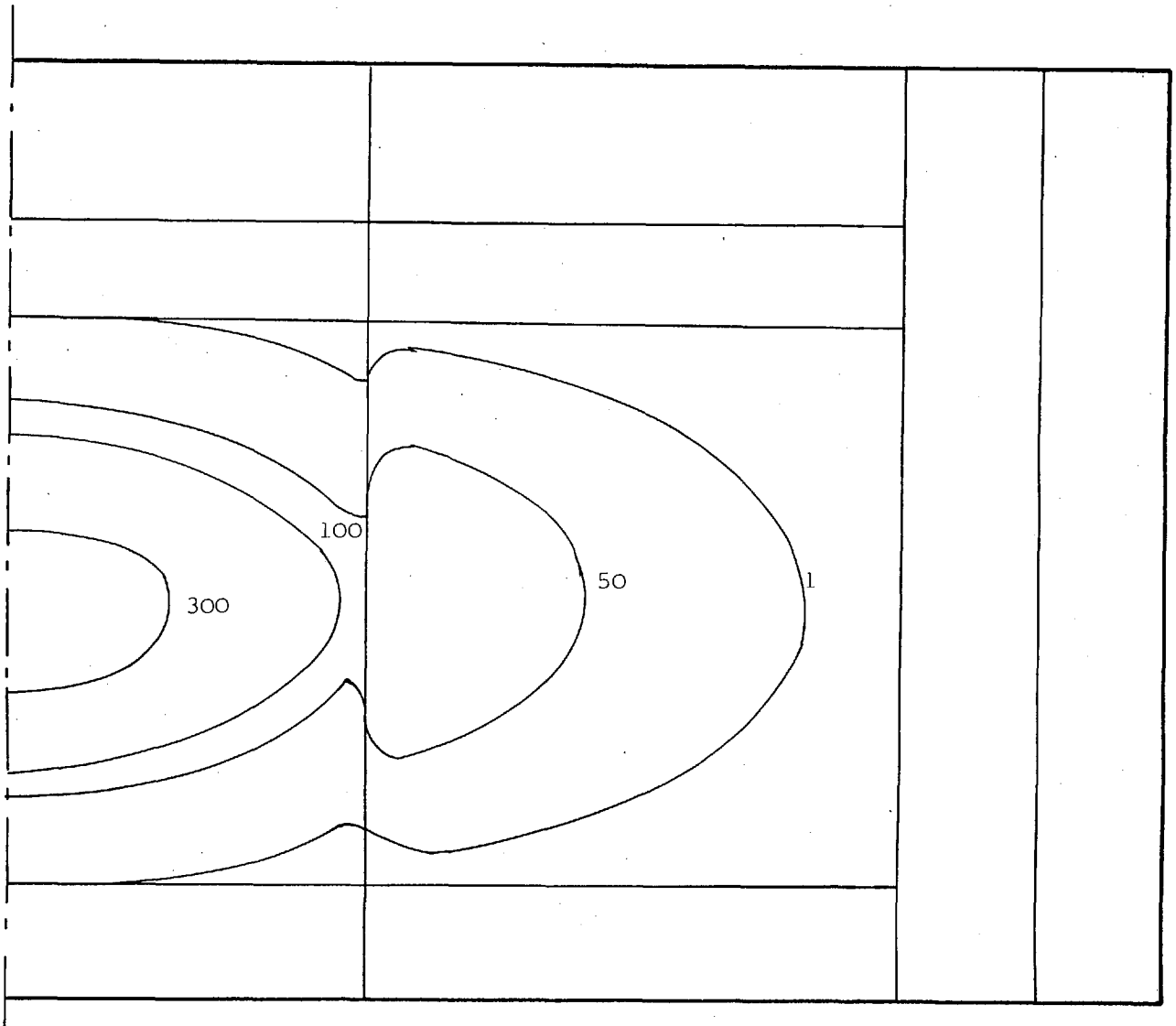


Figure 6.14b Final Pressure Distribution - Core A

2. G.J. Yield

Pressure in bar

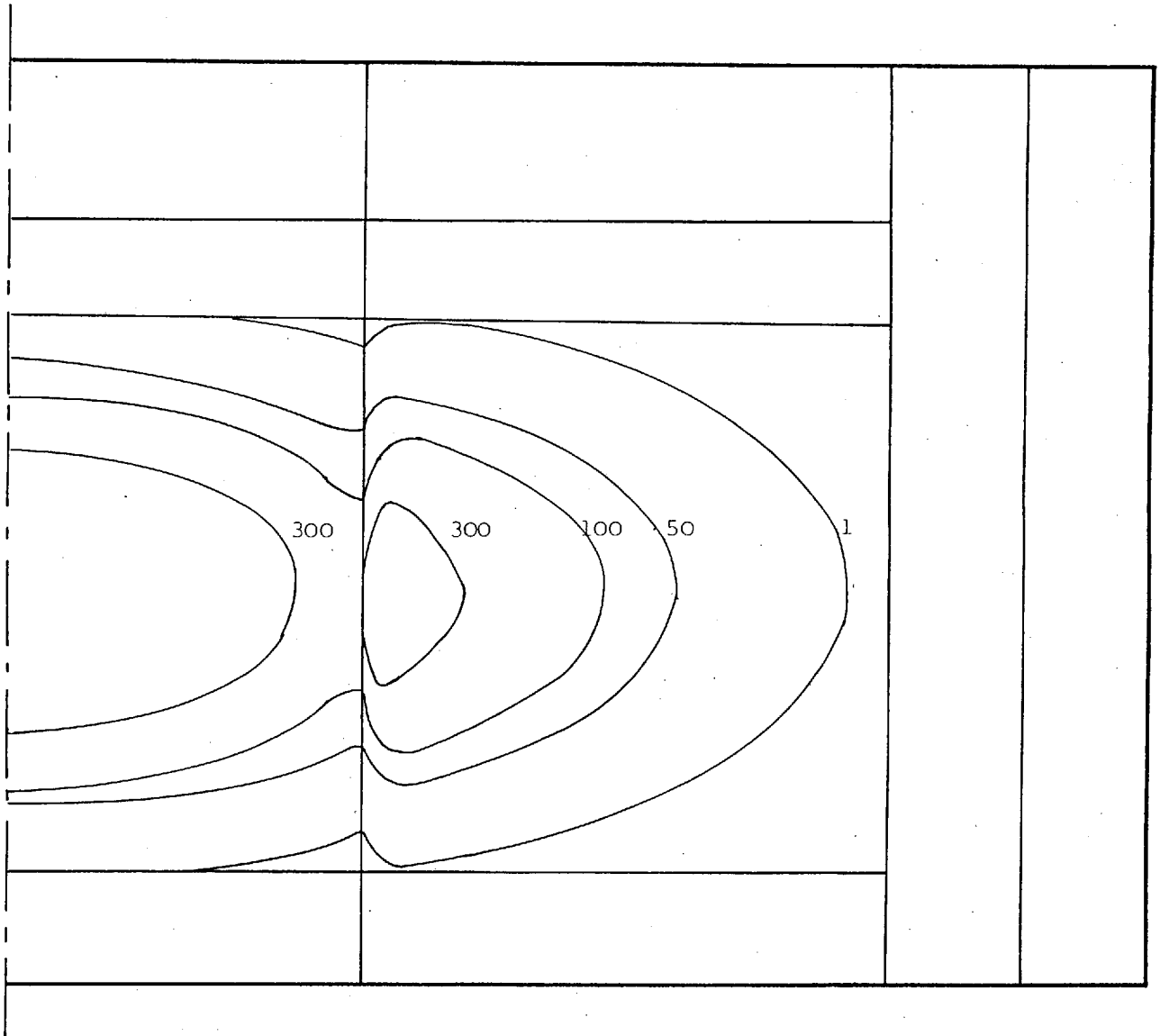


Figure 6.14c Final Pressure Distribution - Core A

5 G.J. Yield

Pressure in bar

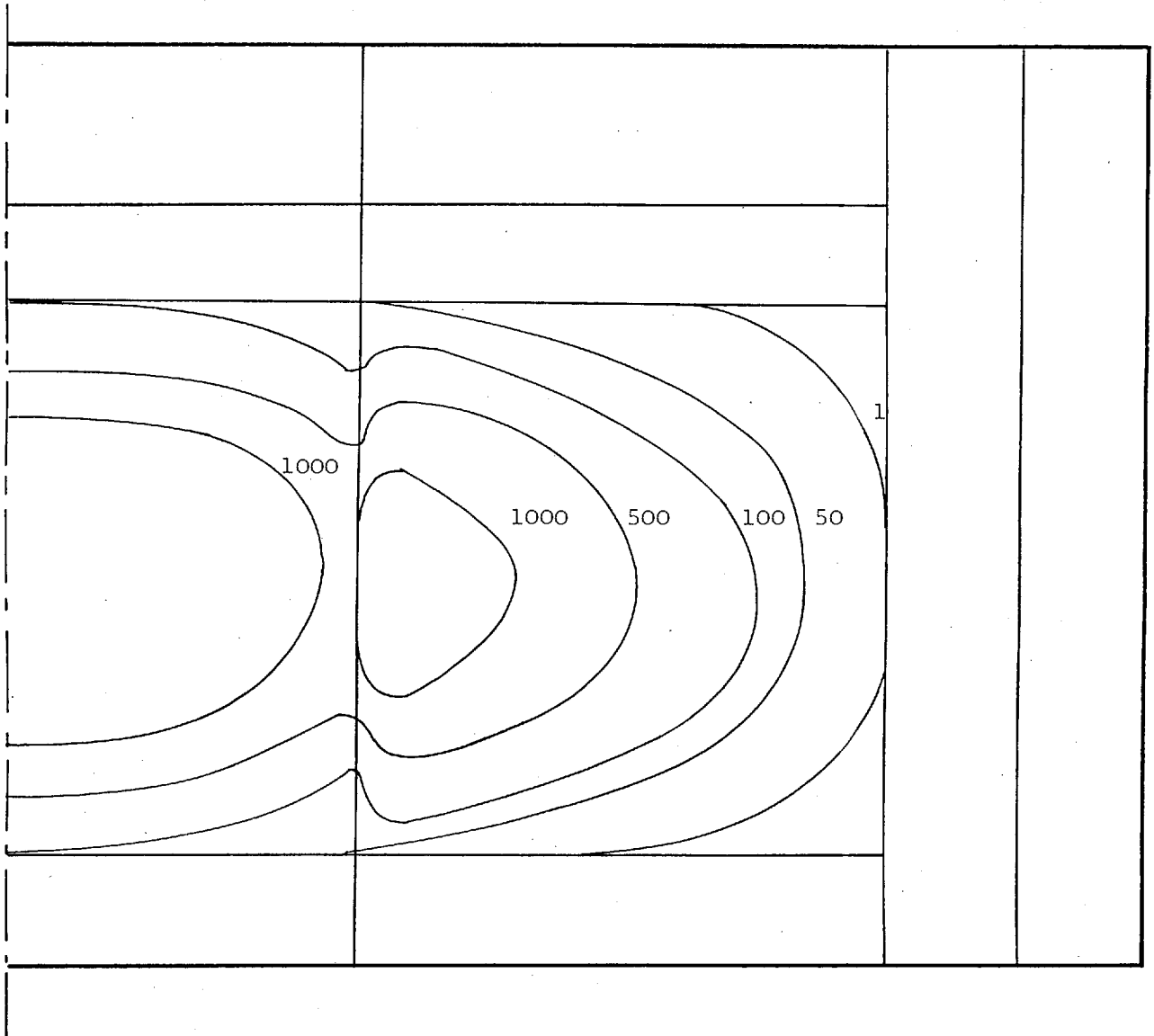


Figure 6.14d Final Pressure Distribution - Core A

10 G.J. Yield

Pressure in bar

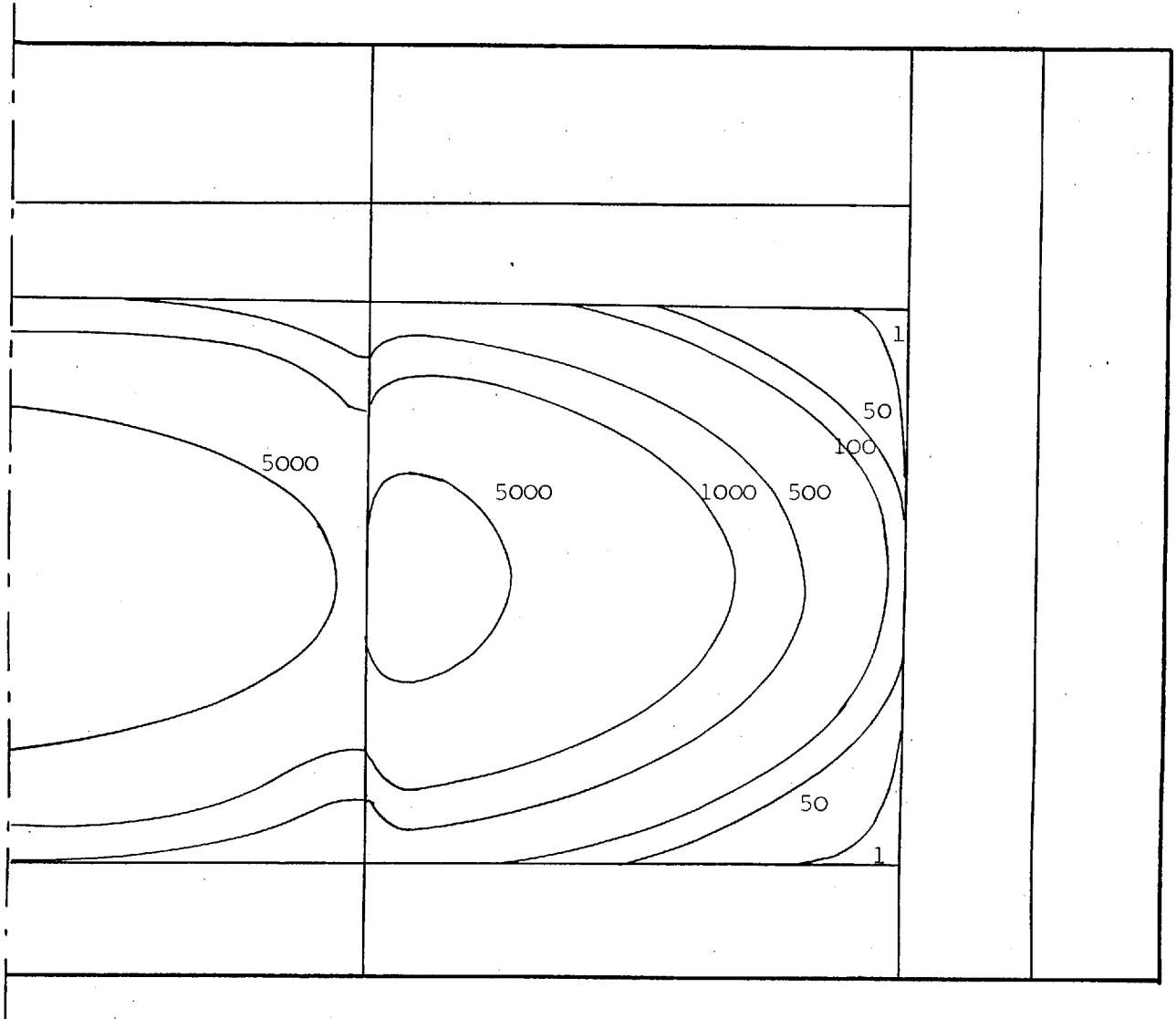


Figure 6.15a Power Histories with Limited Reactivity

Input - Core A

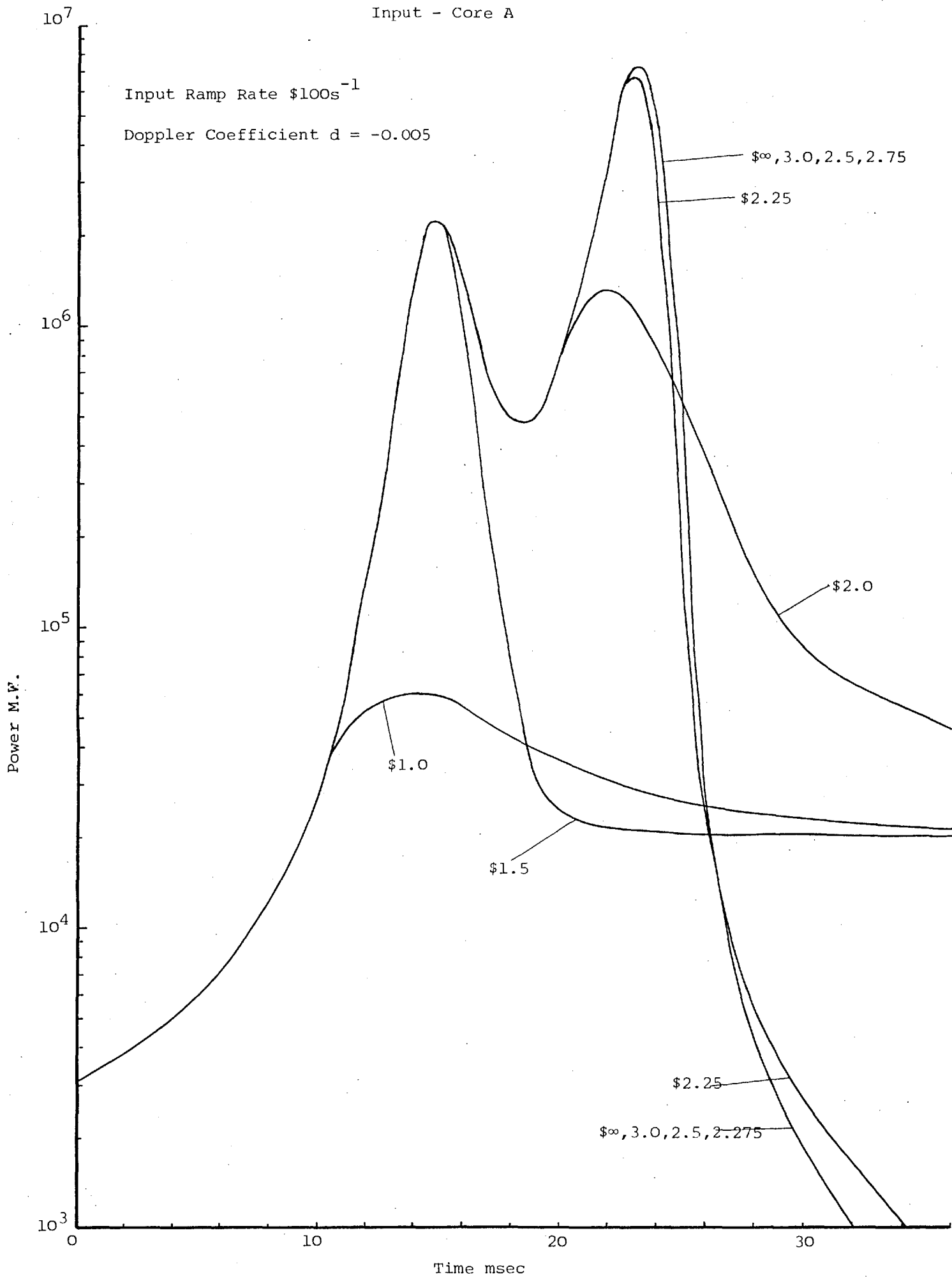


Figure 6.15b Energy Histories with Limited Reactivity

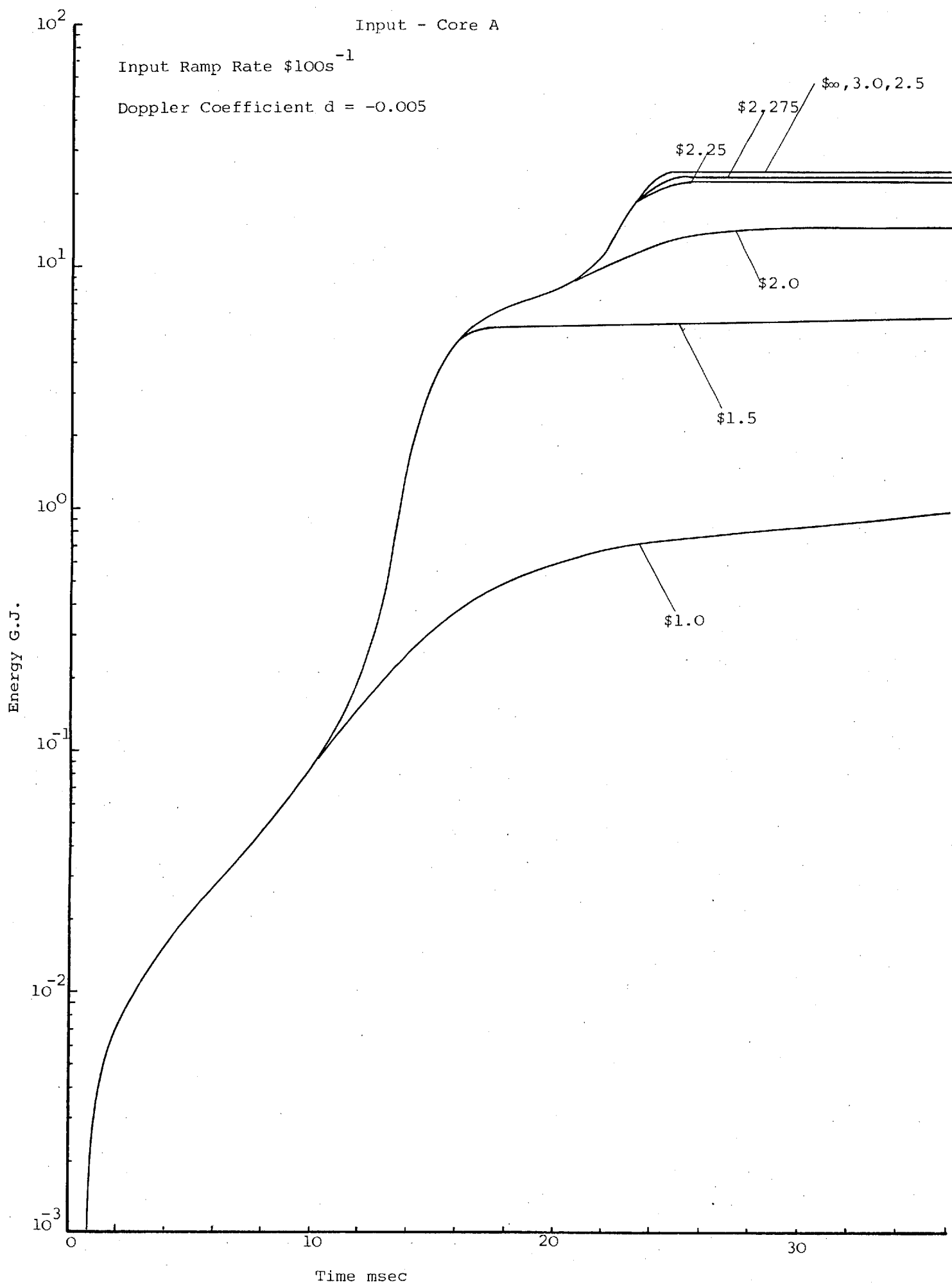


Figure 6.16 Example Non-Linear Reactivity Input Function

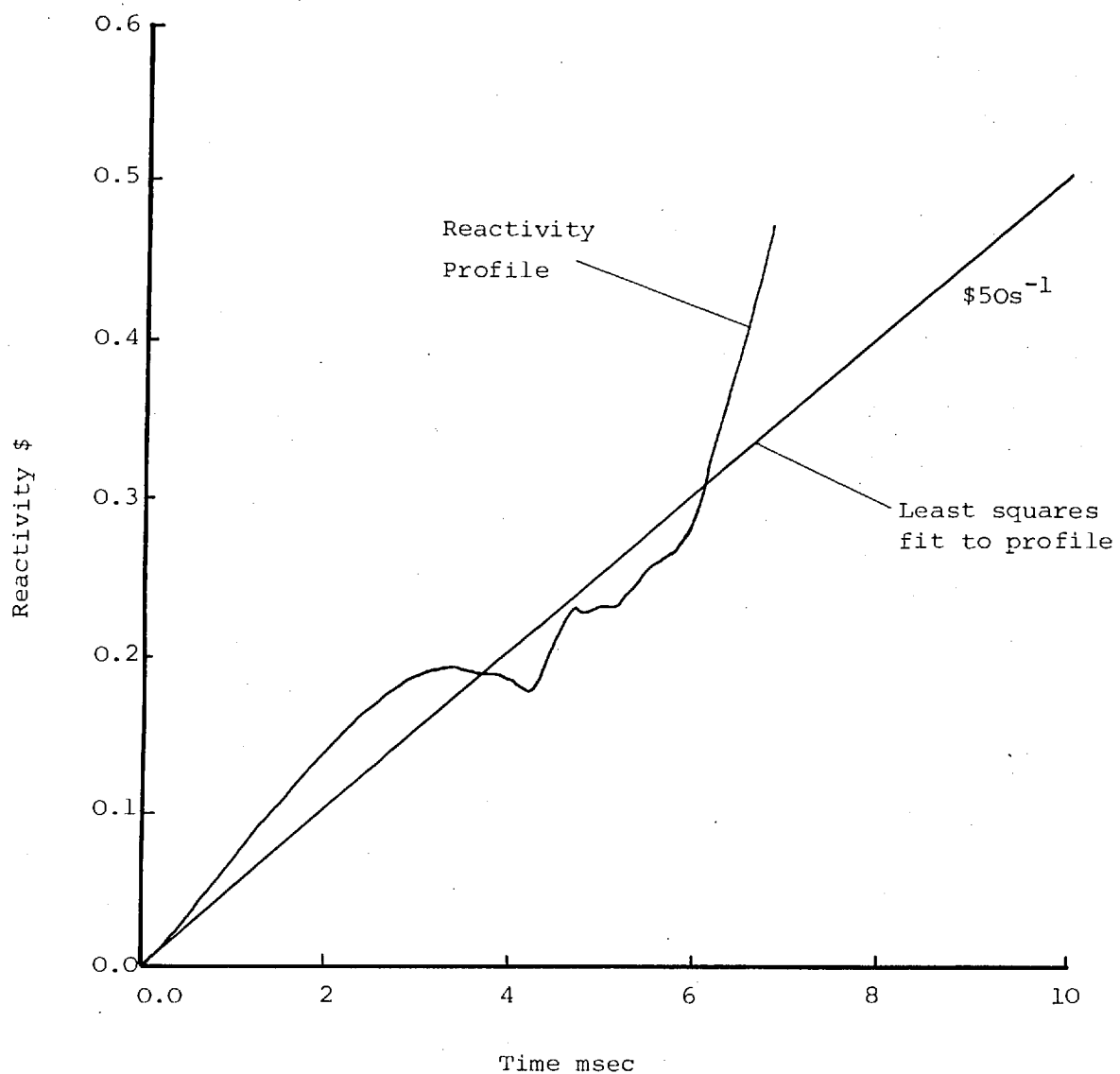


Figure 6.17 Excursion Yields for Piecewise Linear

Reactivity Input - Core A

Input Ramp Rate 100s^{-1} changed to 500s^{-1} at various points in time, Doppler Coefficient $d = -0.005$

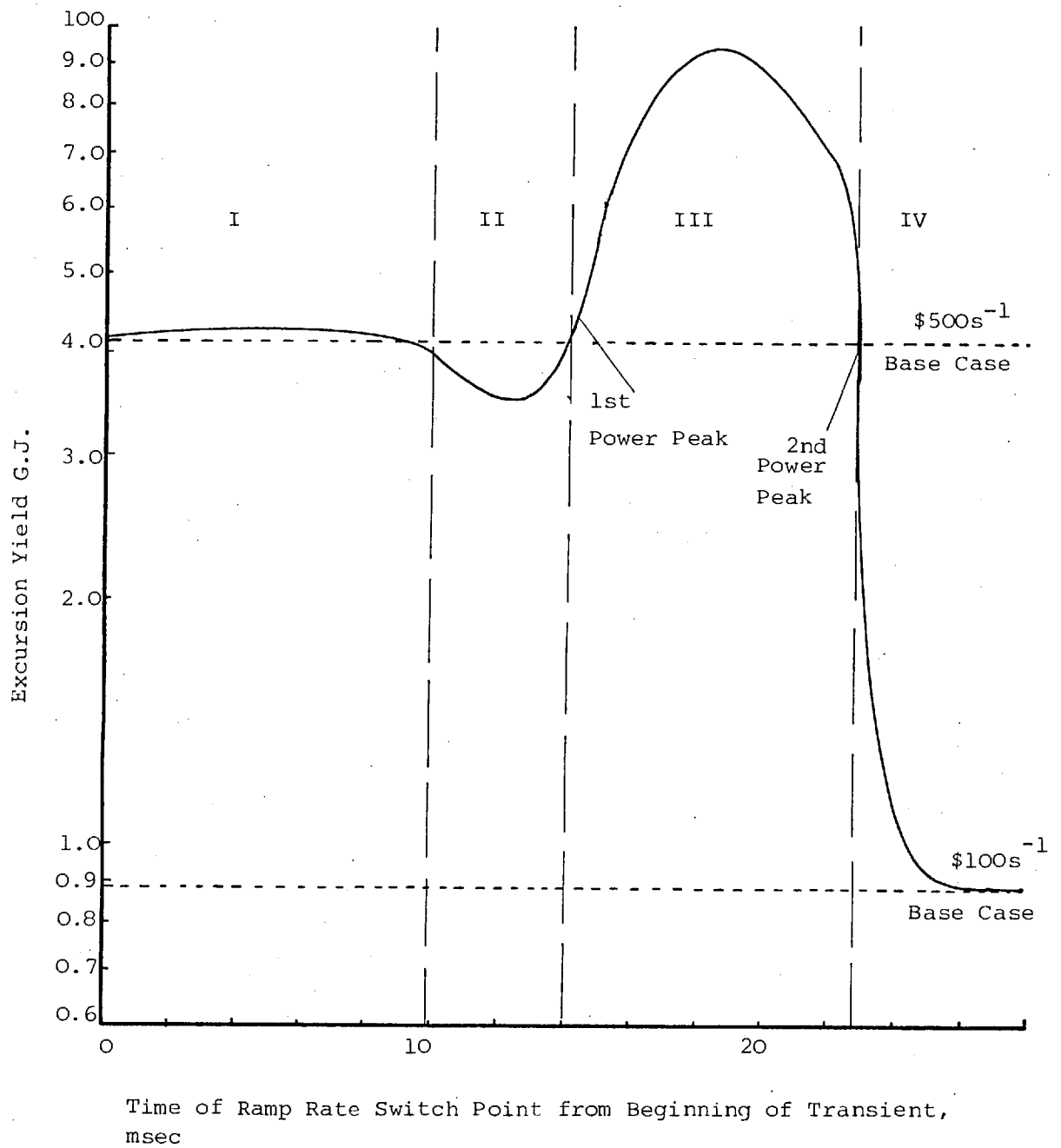


Figure 6.18 Reactivity Histories for Piecewise Linear Reactivity Input - Core A

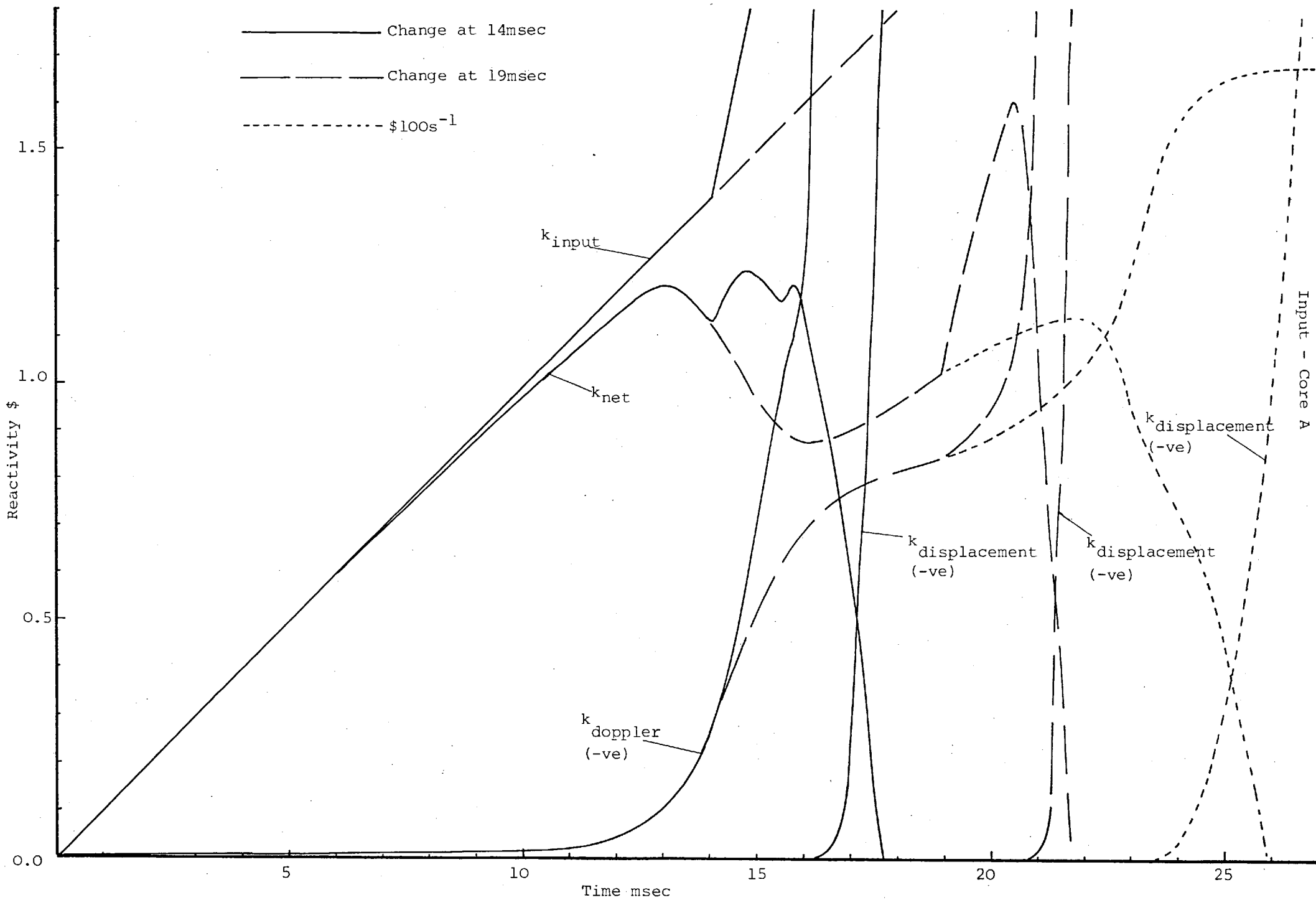


Figure 6.18b Power History for Piecewise Linear Reactivity

Input - Core A

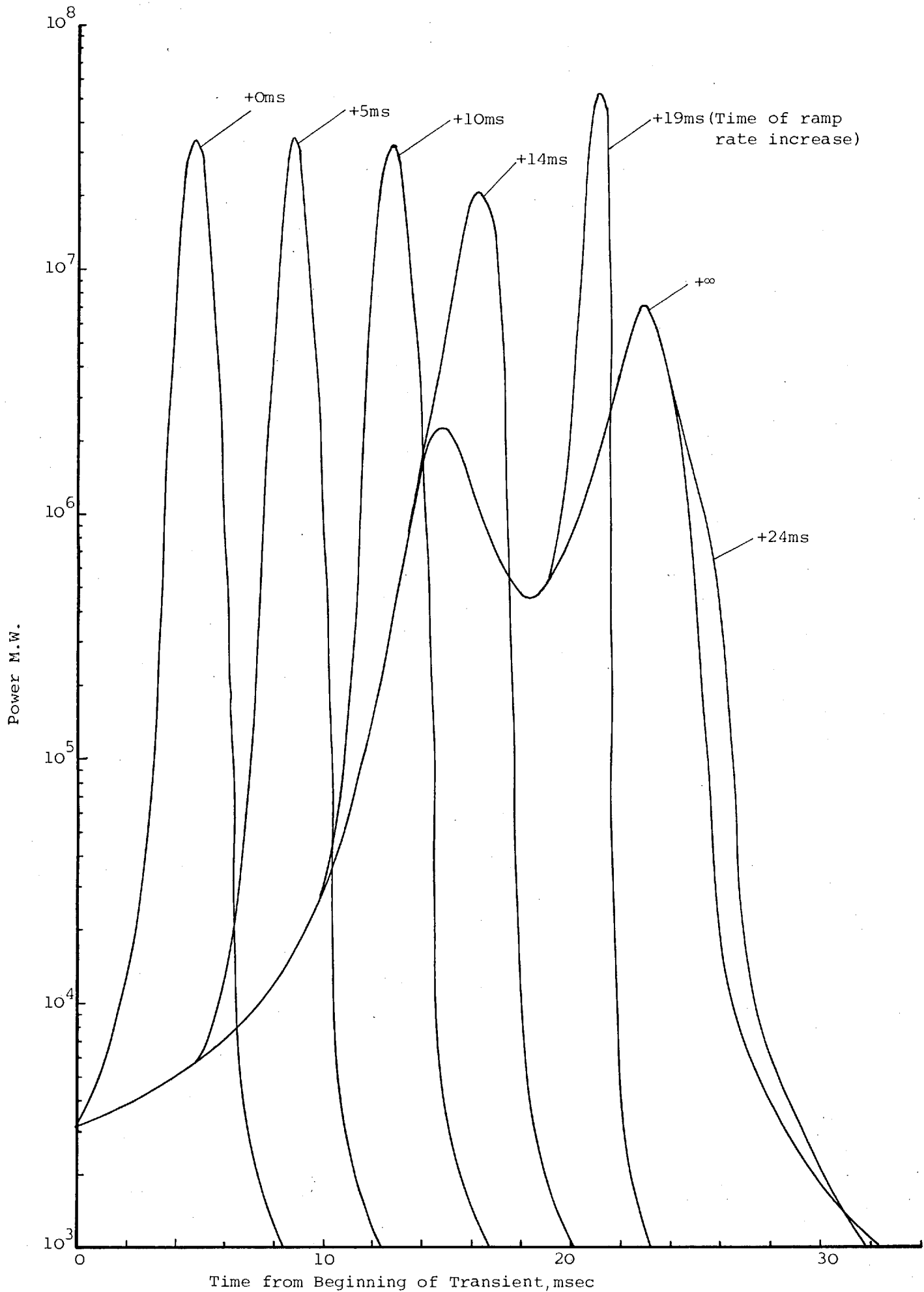


Figure 6.18c Energy History for Piecewise Linear Reactivity

Input - Core A

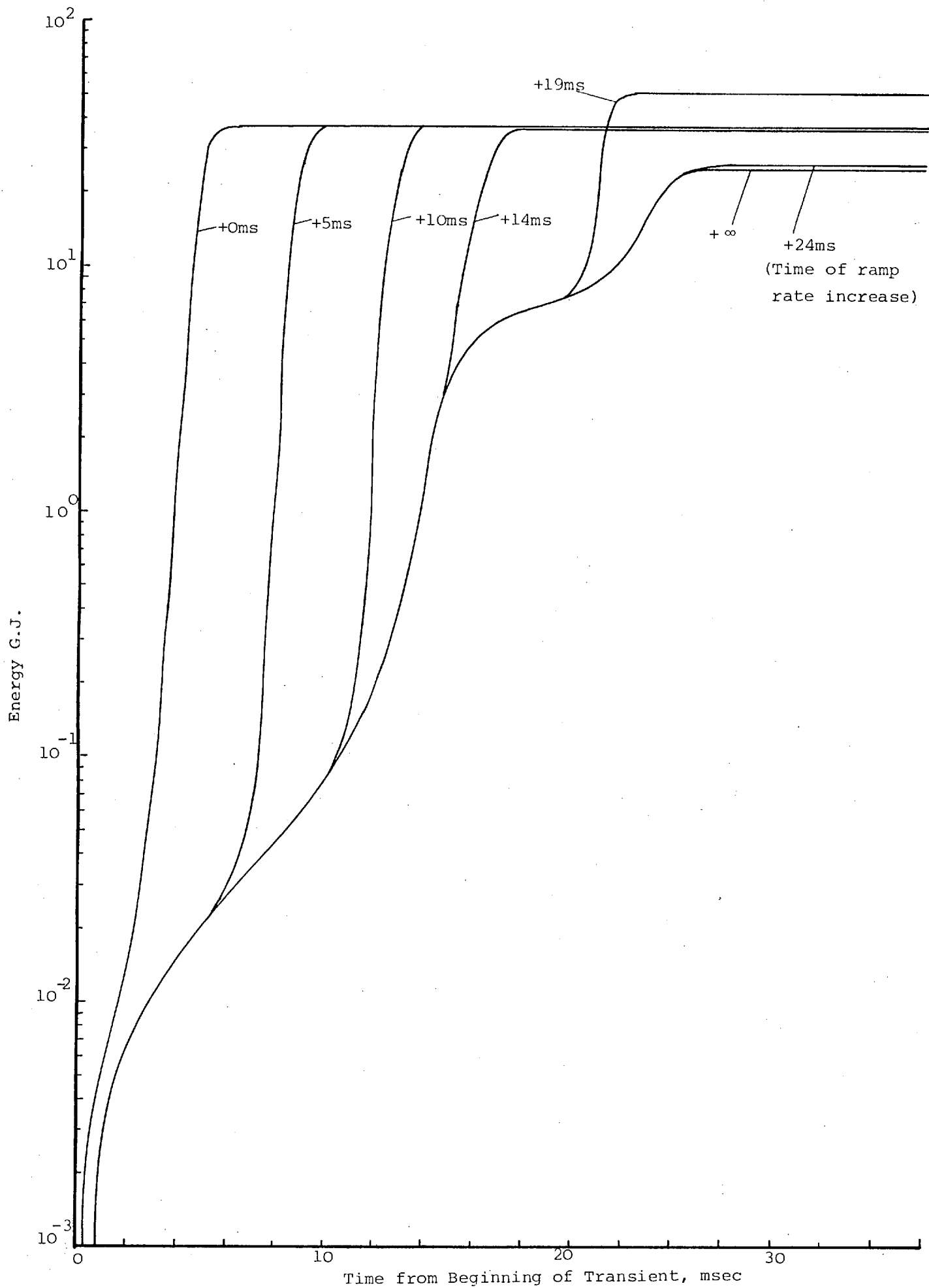


Fig. 6.19a Excursion Yields for Step Reactivity Input - Core A

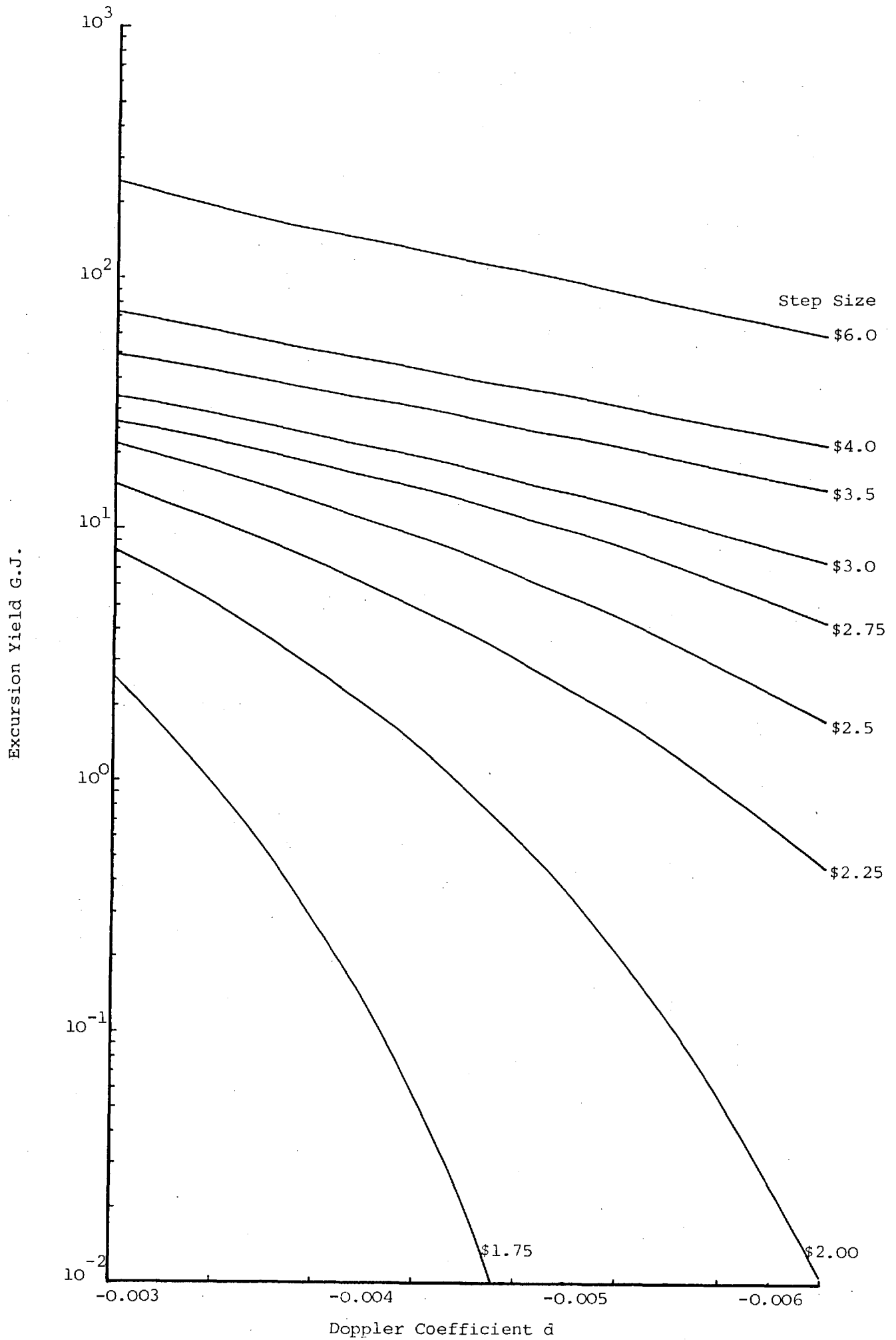


Figure 6.19b Excursion Yield for Step Reactivity Input - Core A

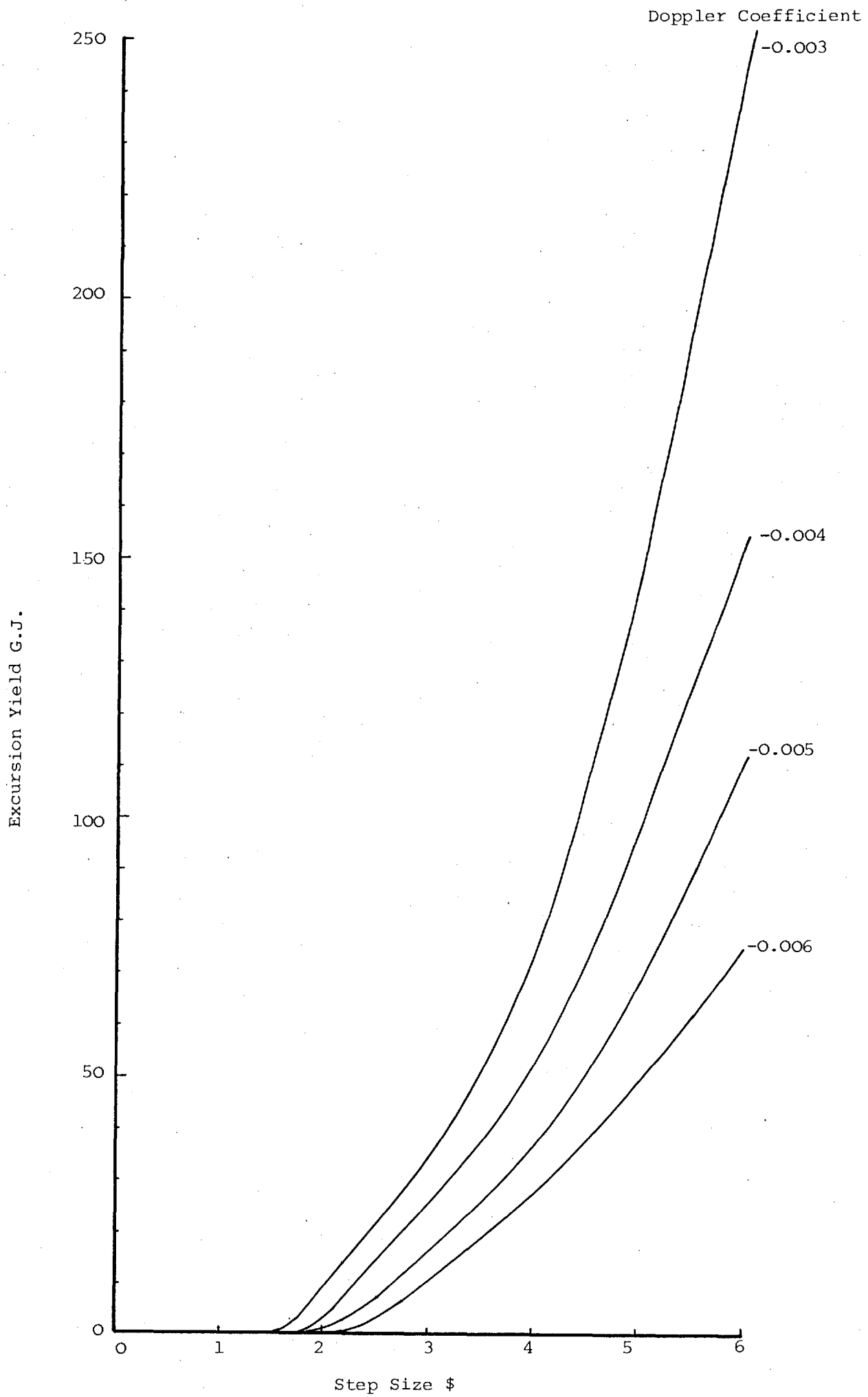


Figure 6.20a Internal Energy-Temperature Relationship for UO_2

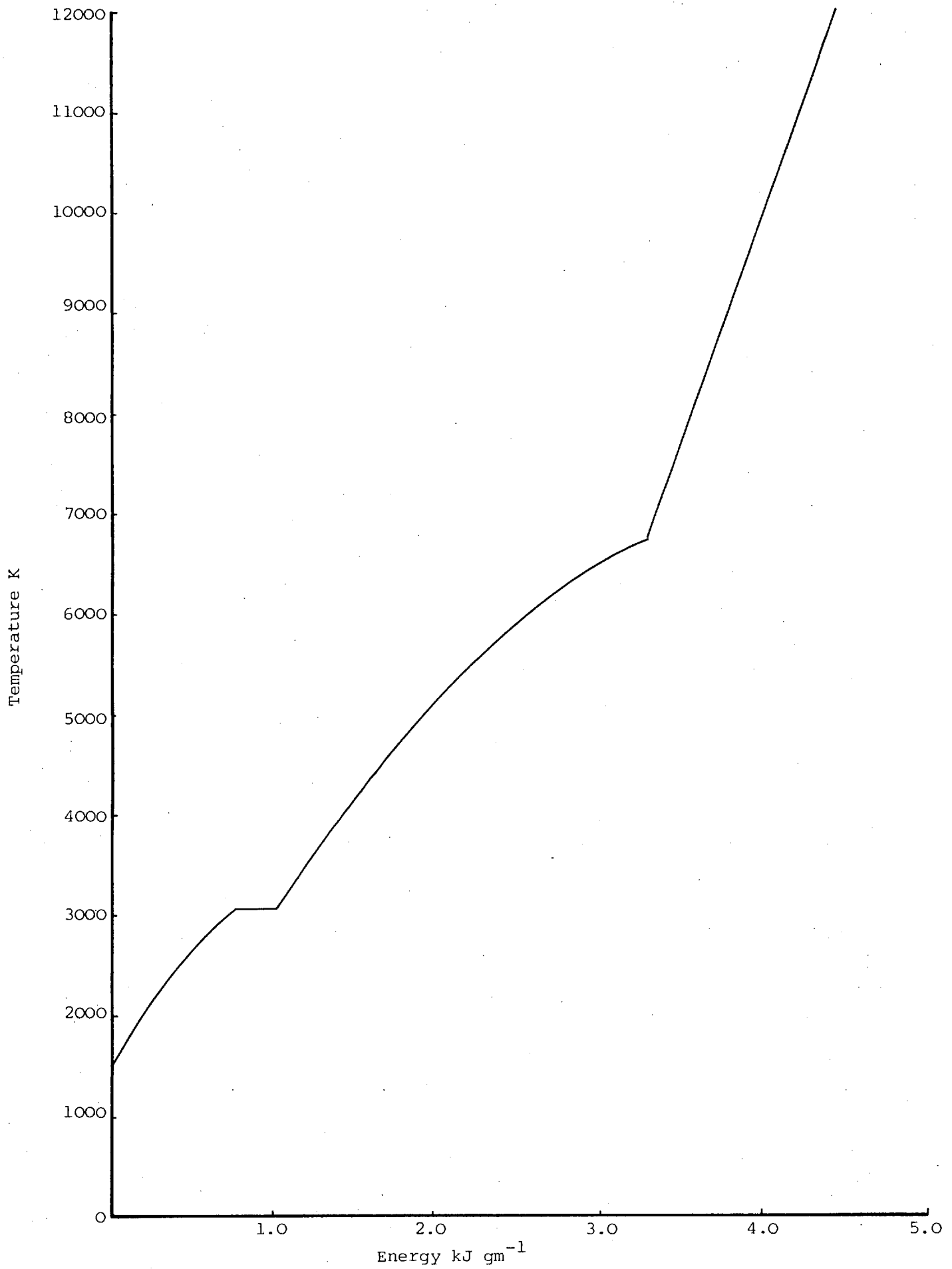


Figure 6.20b Internal Energy - Pressure Relationship for UO_2

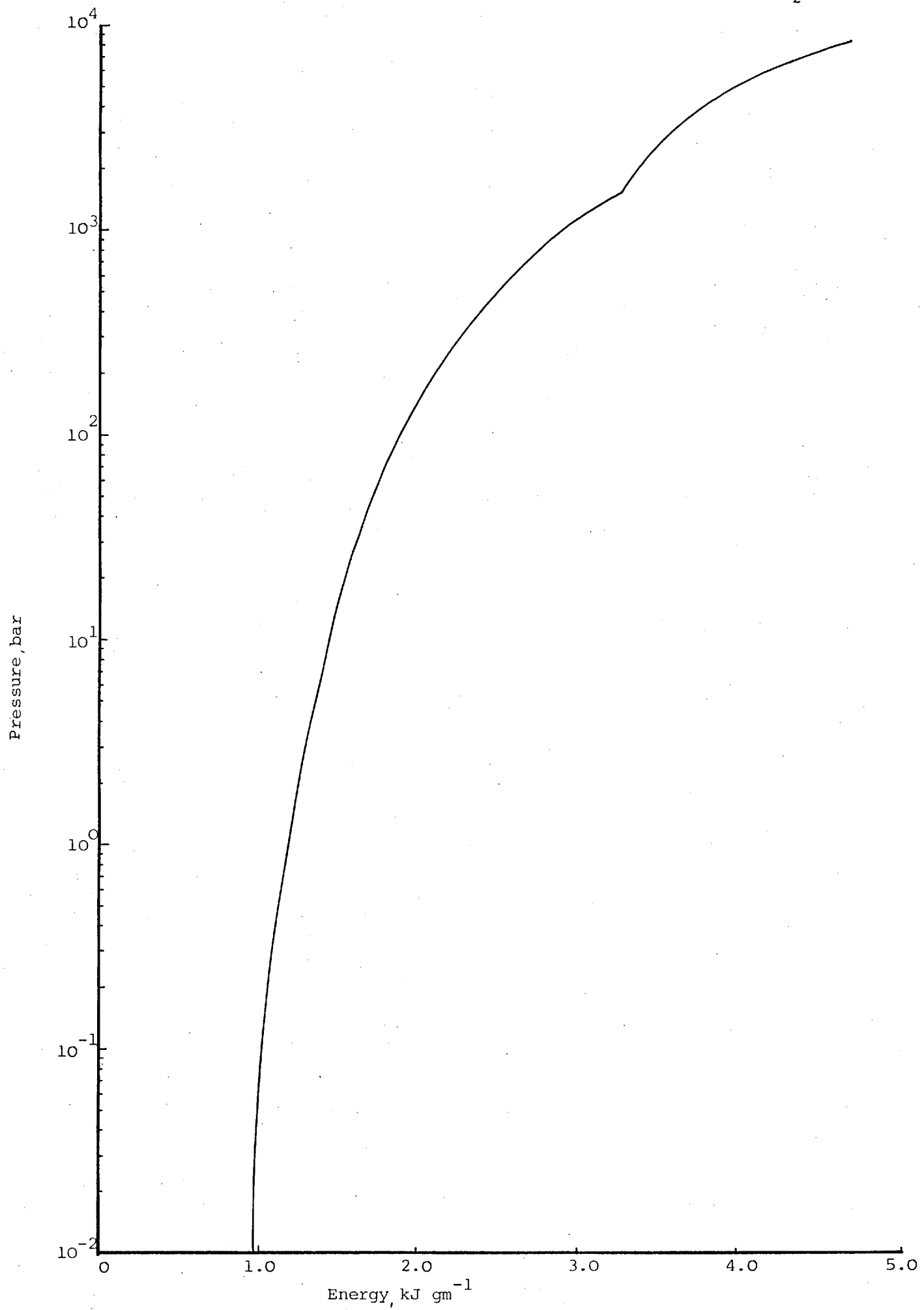


Figure 6.20c Internal Energy - Pressure Relationship for UO_2
with Allowance for Fission Product Pressure

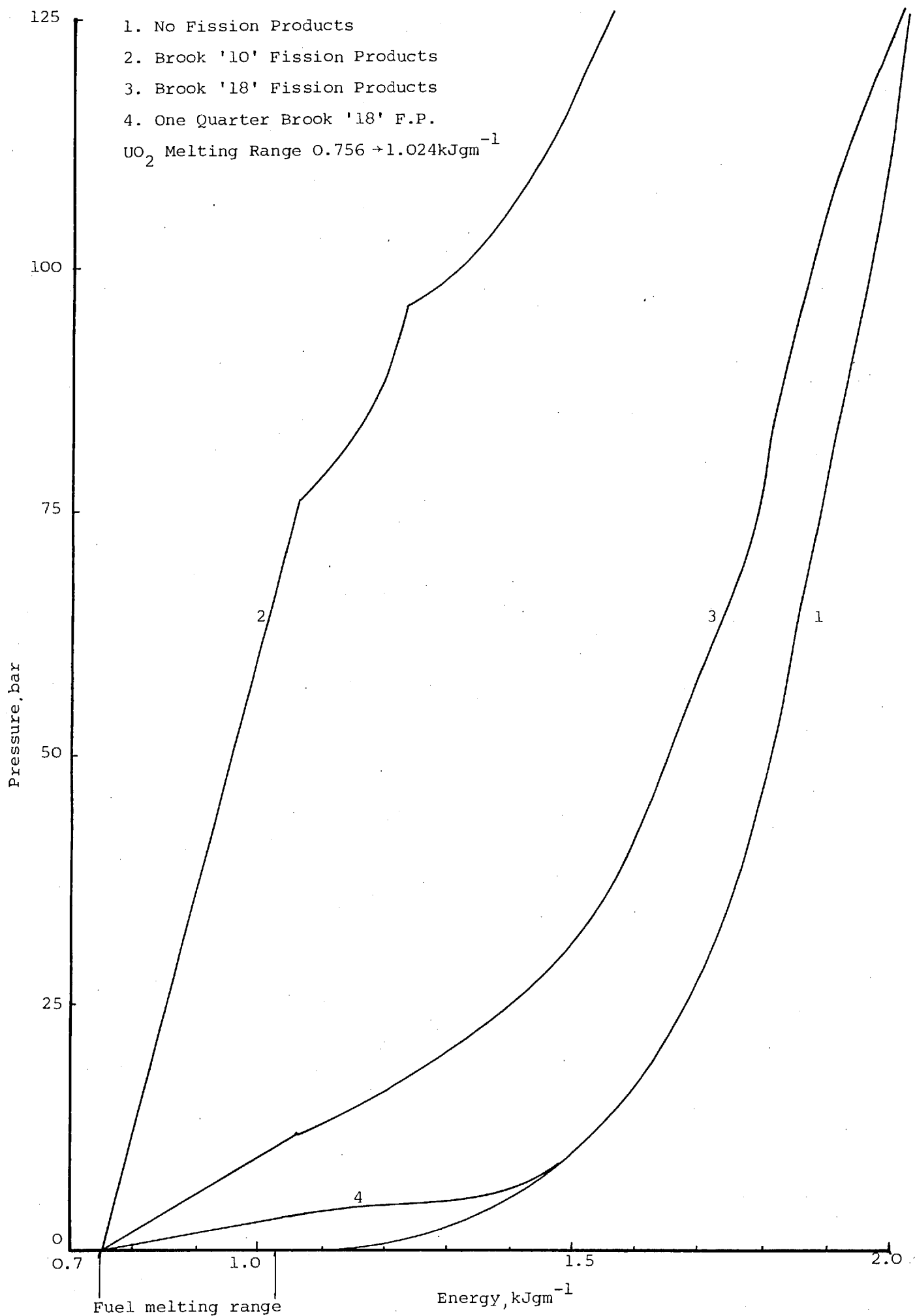


Figure 6.21 Disassembly Feedbacks with Fission Products - Core A

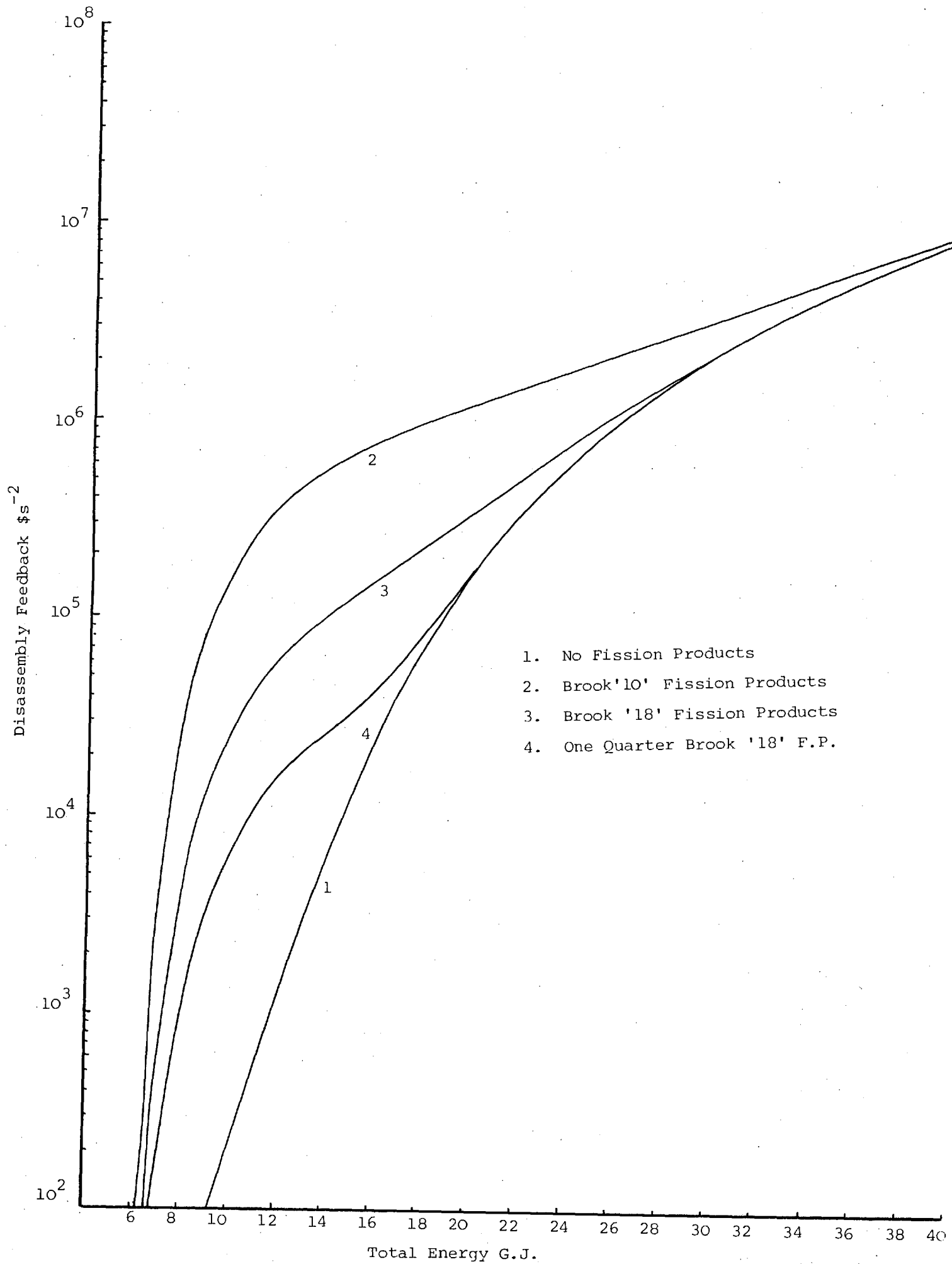


Figure 6.22a Effect of Fission Products on Excursion Yield -

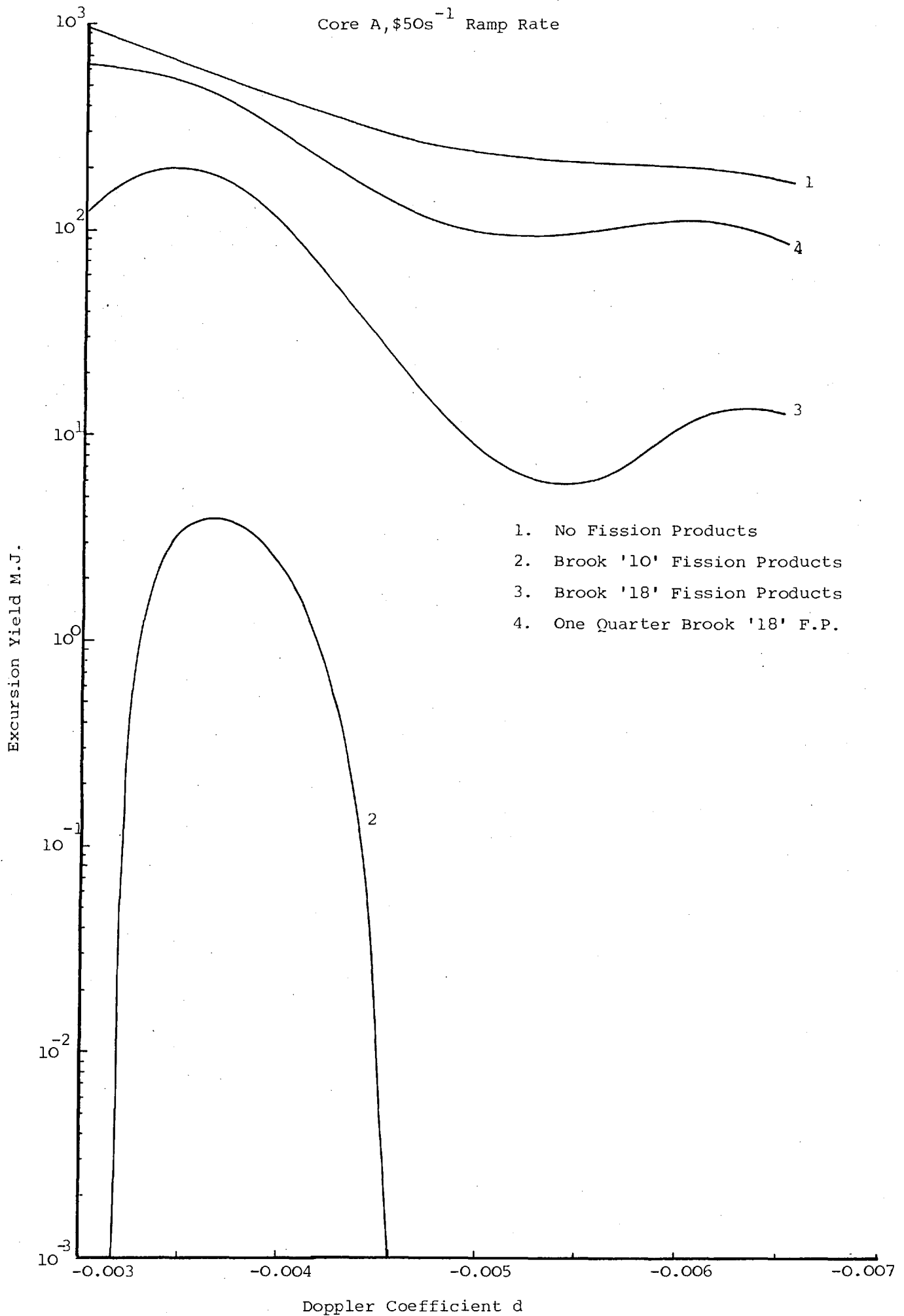


Figure 6.22b Effect of Fission Products on Excursion Yield -

Core A, 100s^{-1} Ramp Rate

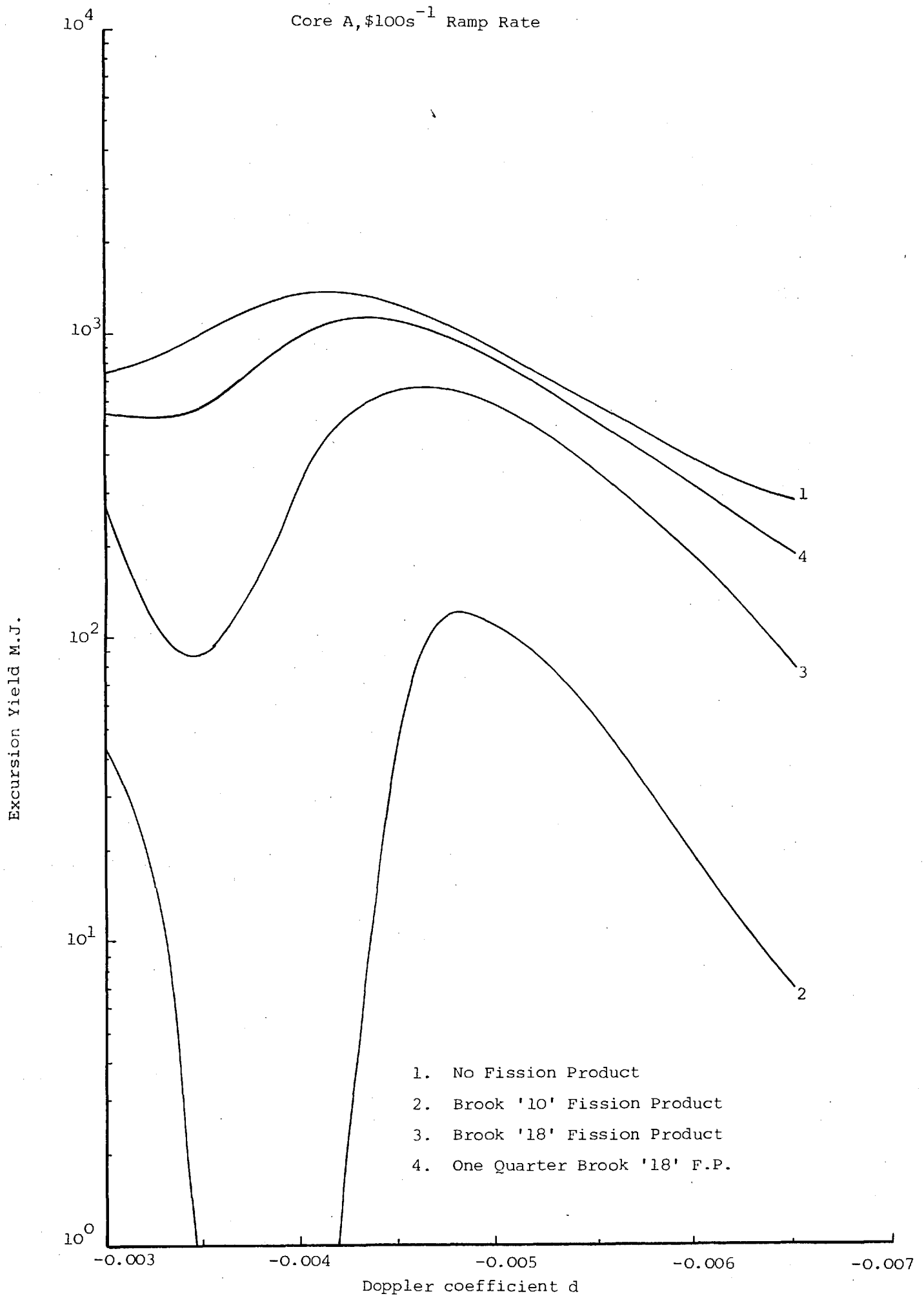


Figure 6.22c Effect of Fission Products on Excursion Yield -

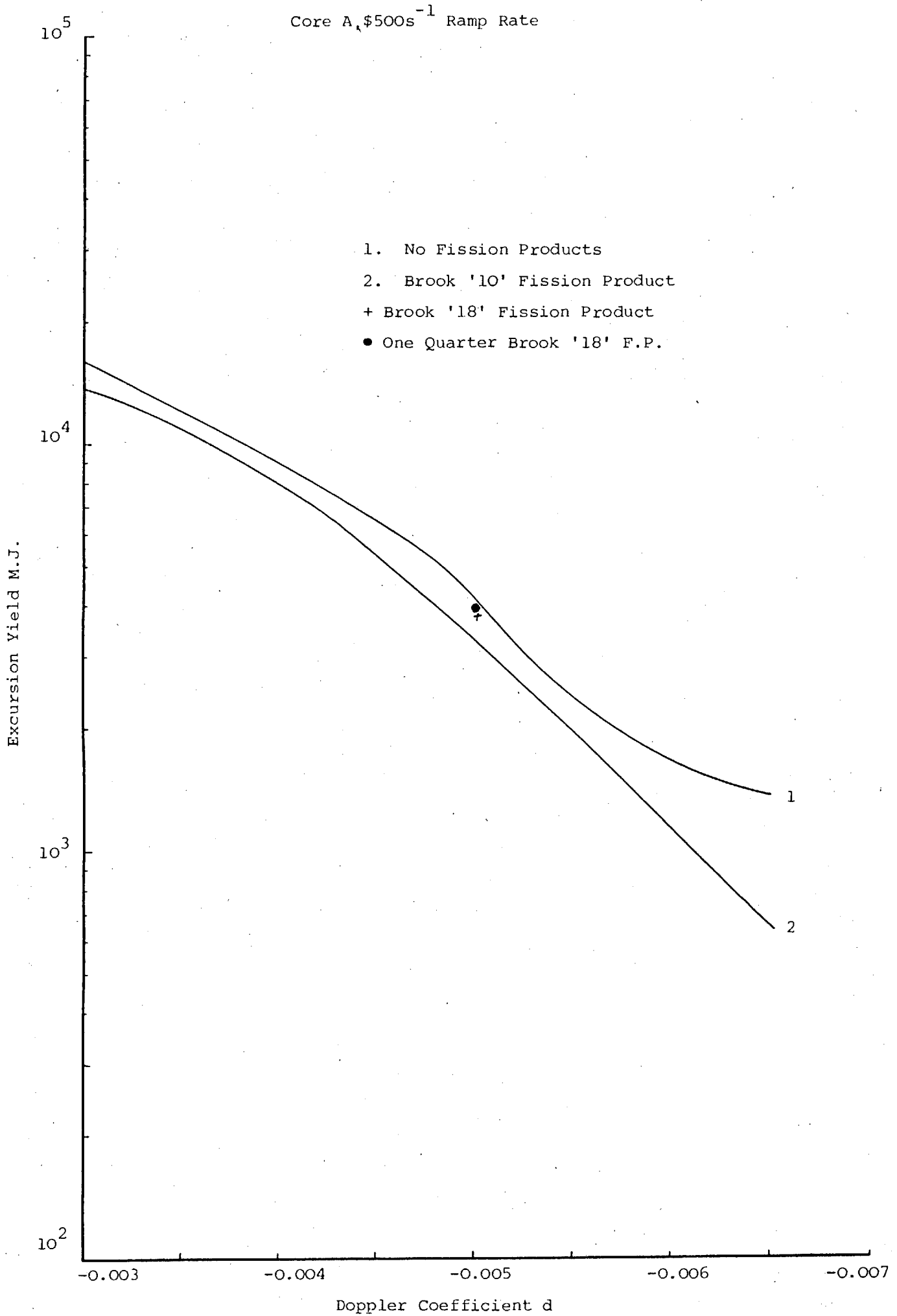


Figure 6.23a Reactivity History with Fission Products

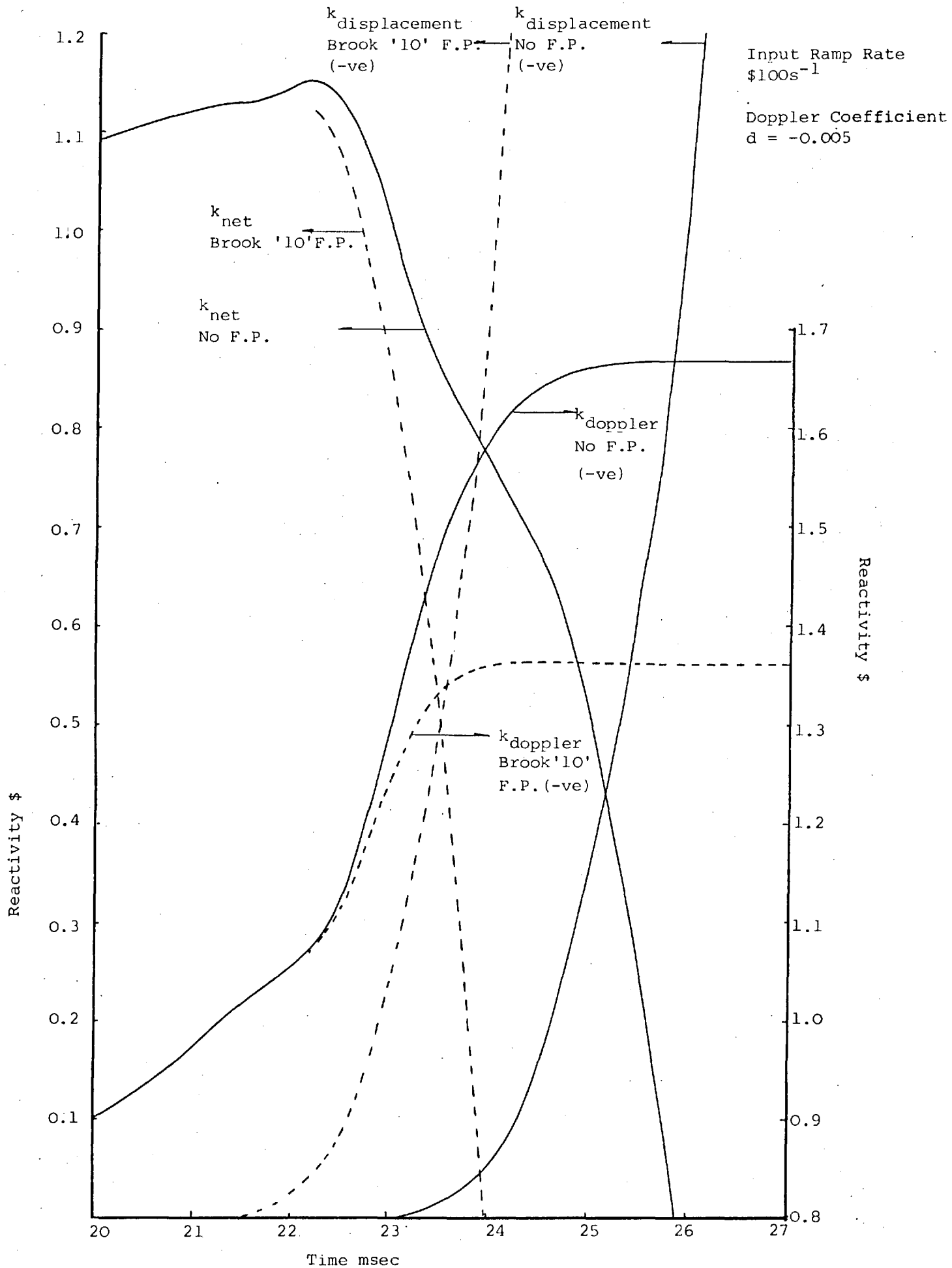


Figure 6.23b Power Histories with Fission Product - Core A

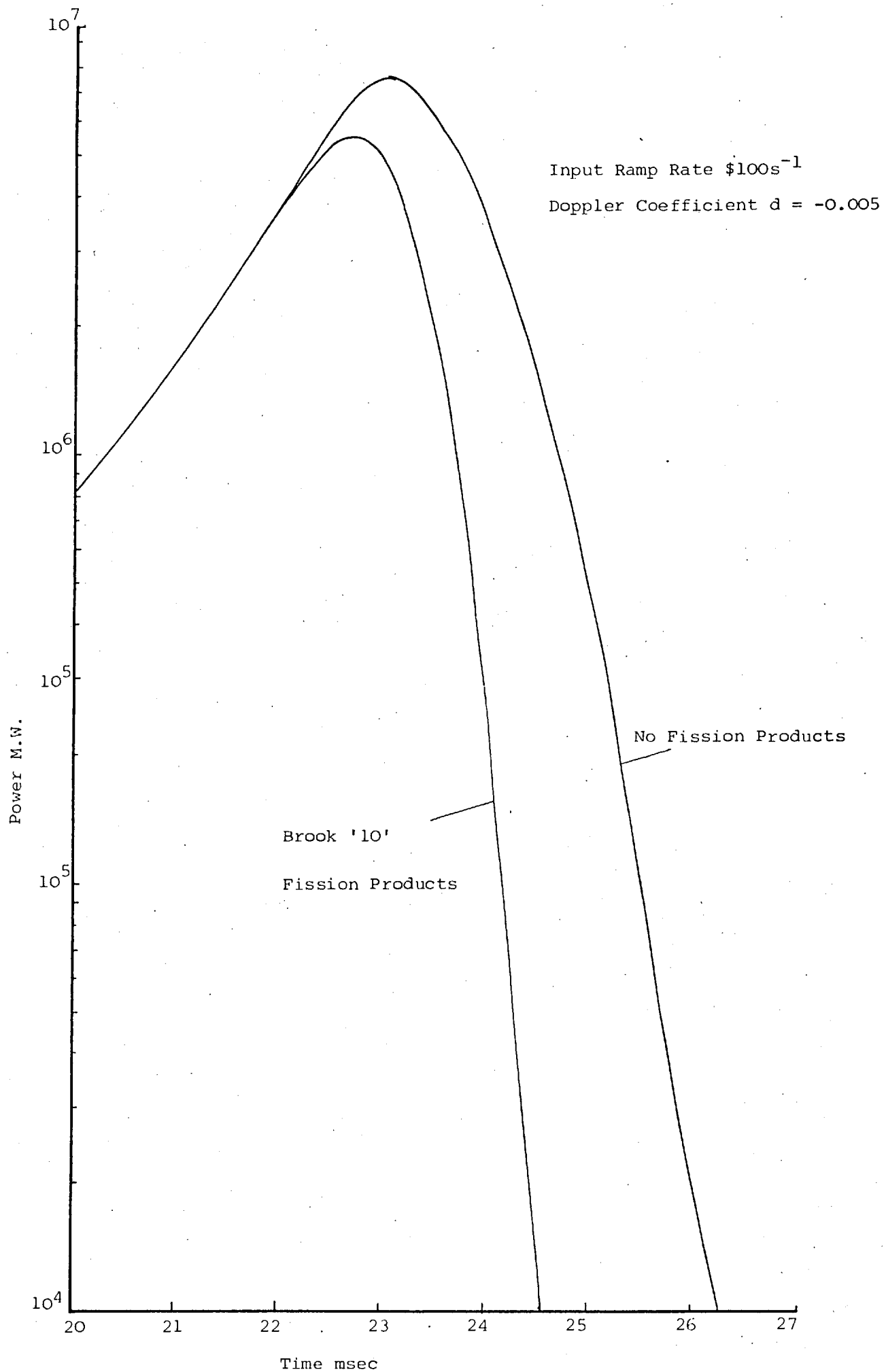


Figure 6.23c Energy History with Fission Product - Core A

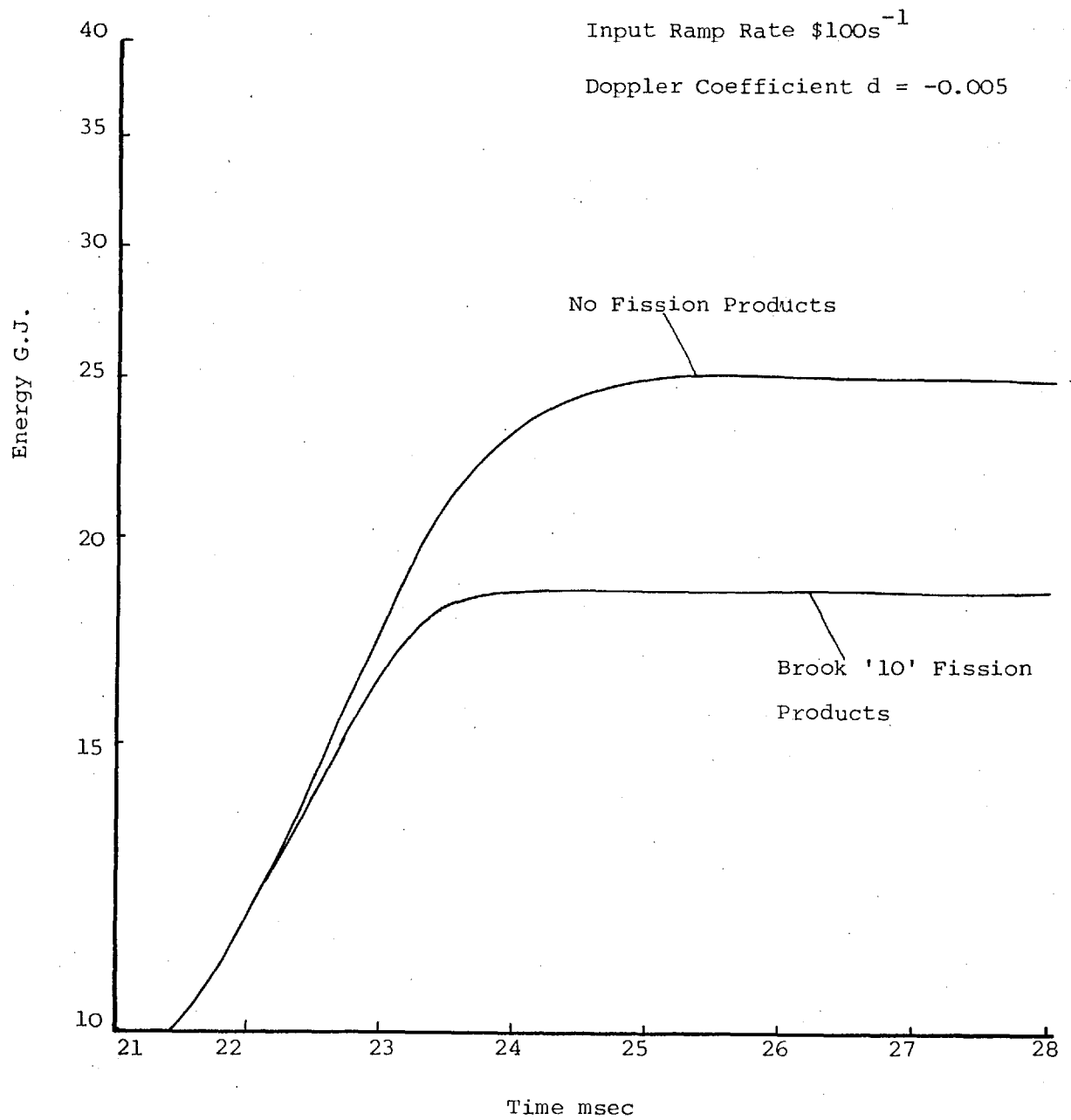


Figure 6.24 Effect of Radial Restraint on Disassembly Feedback -

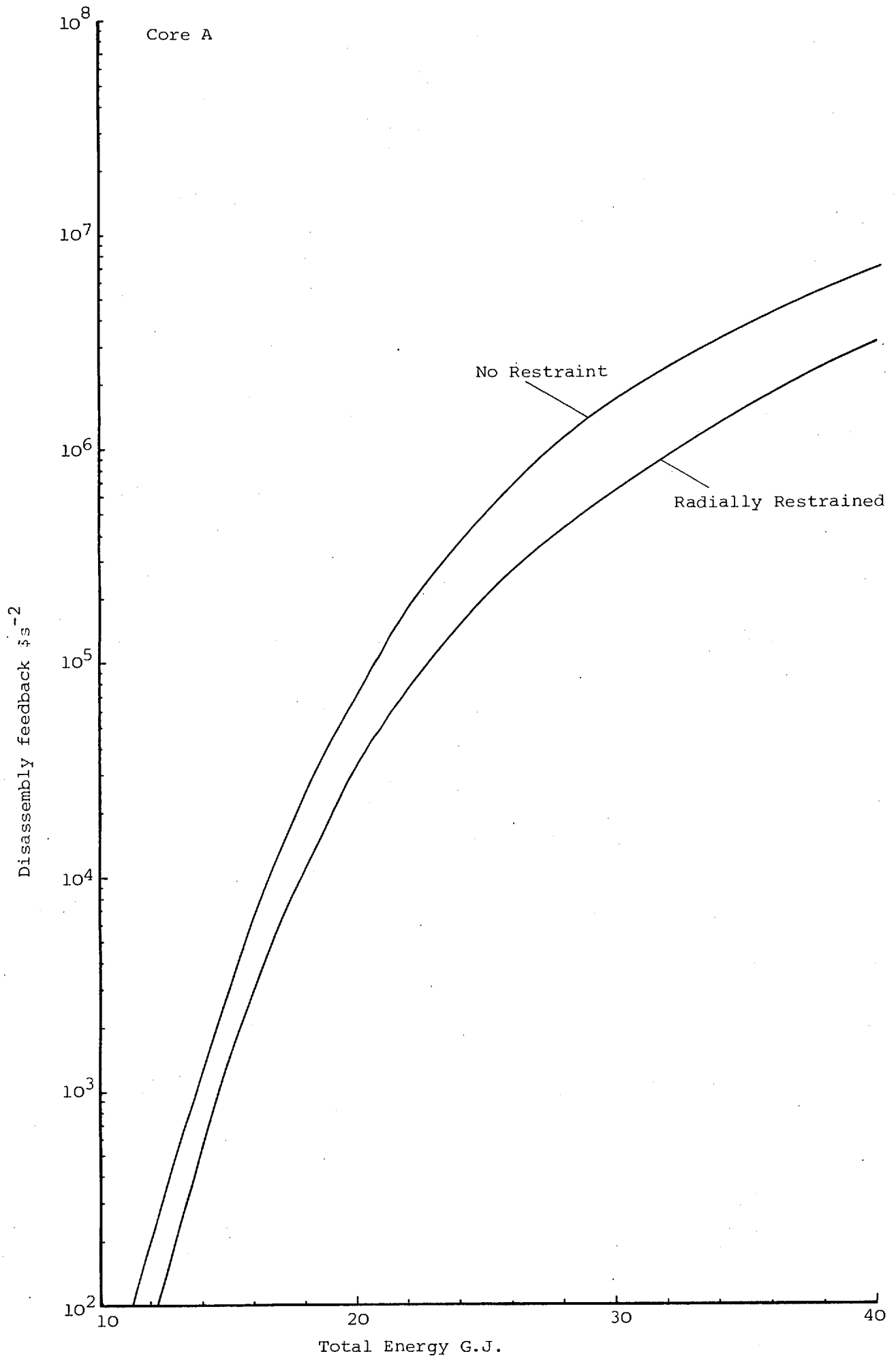


Figure 6.25 Effect of Radial Restraint on Excursion

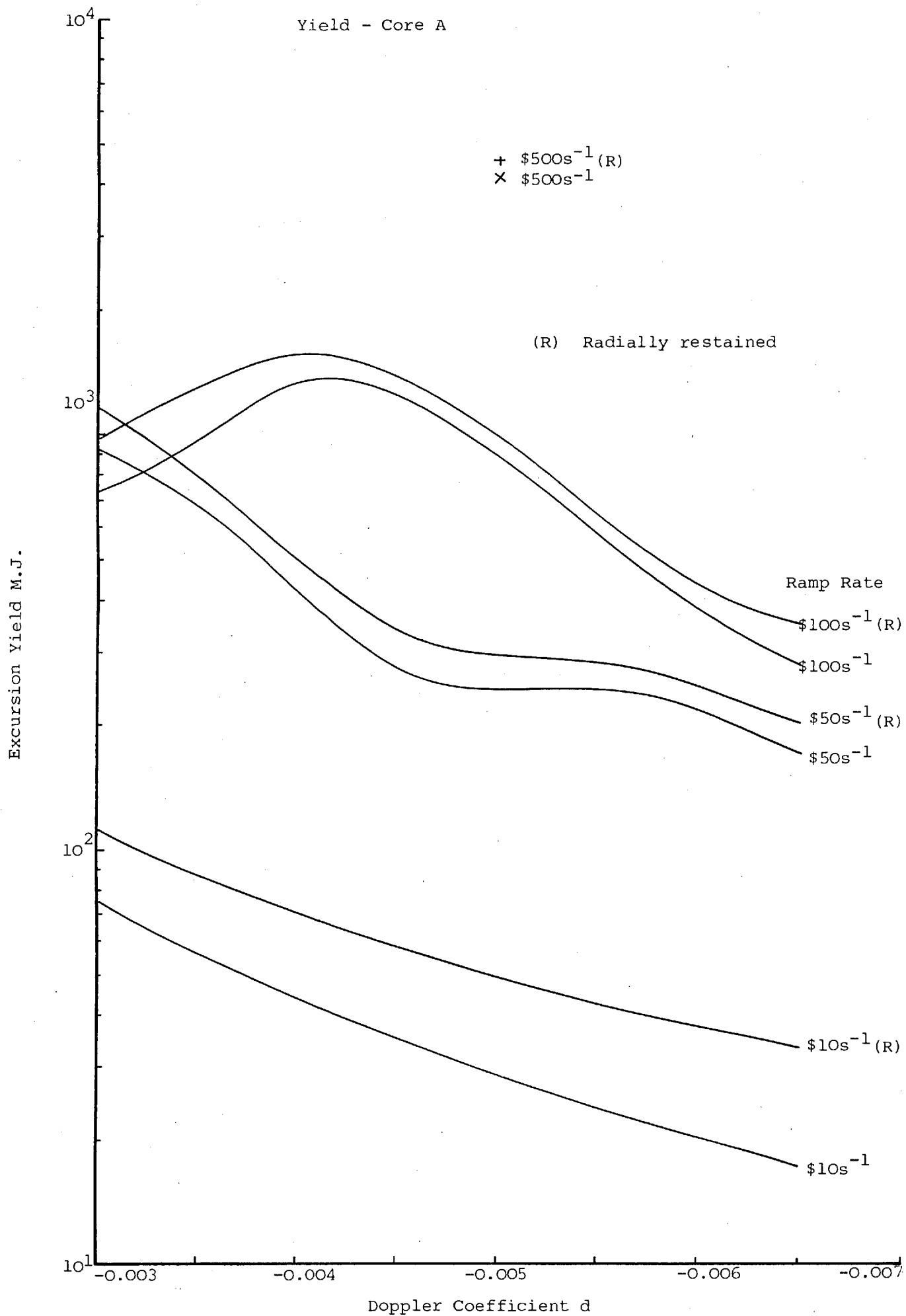


Figure 6.26a Reactivity History with Radial Restraint -

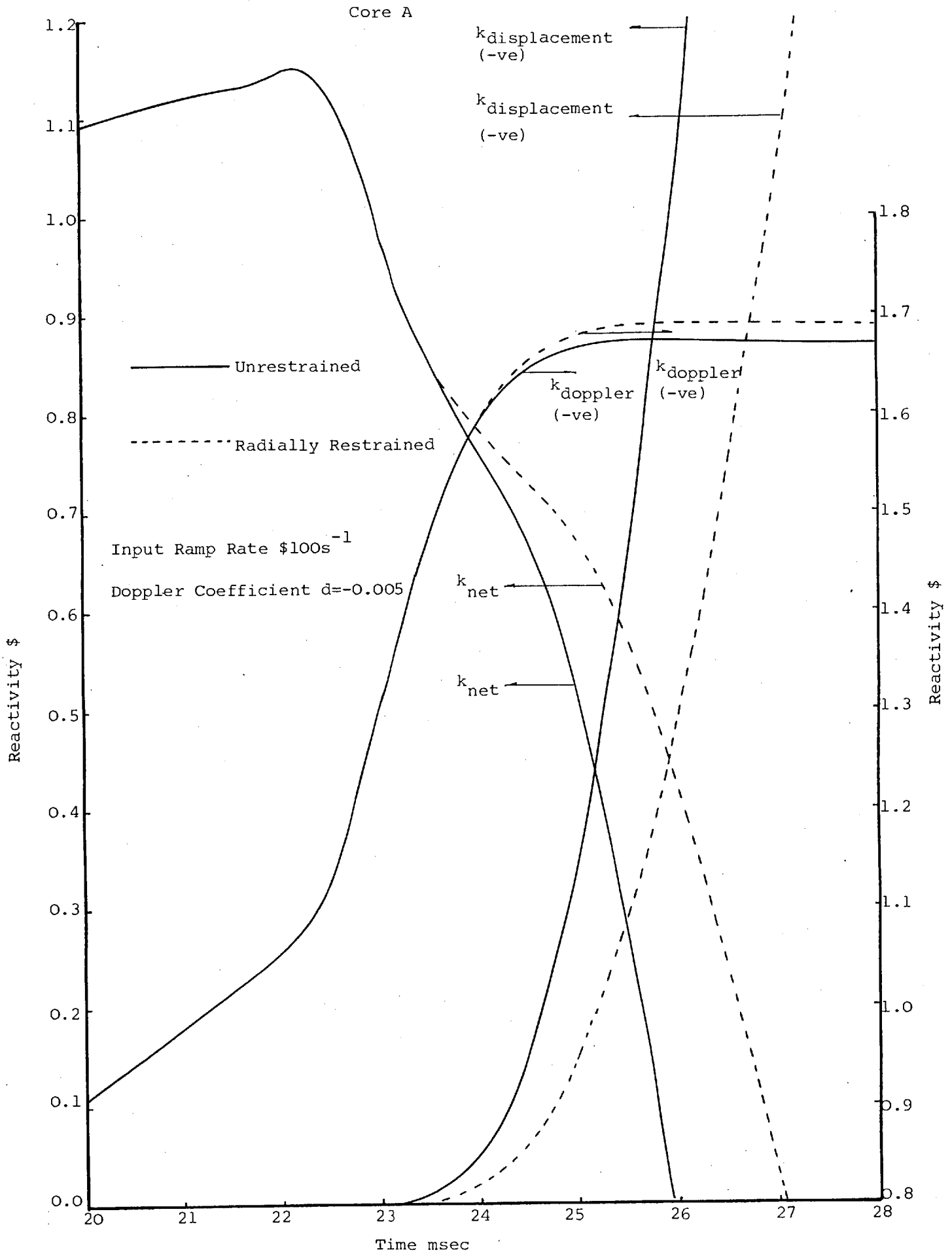


Figure 6.26b Power History with Radial Restraint -

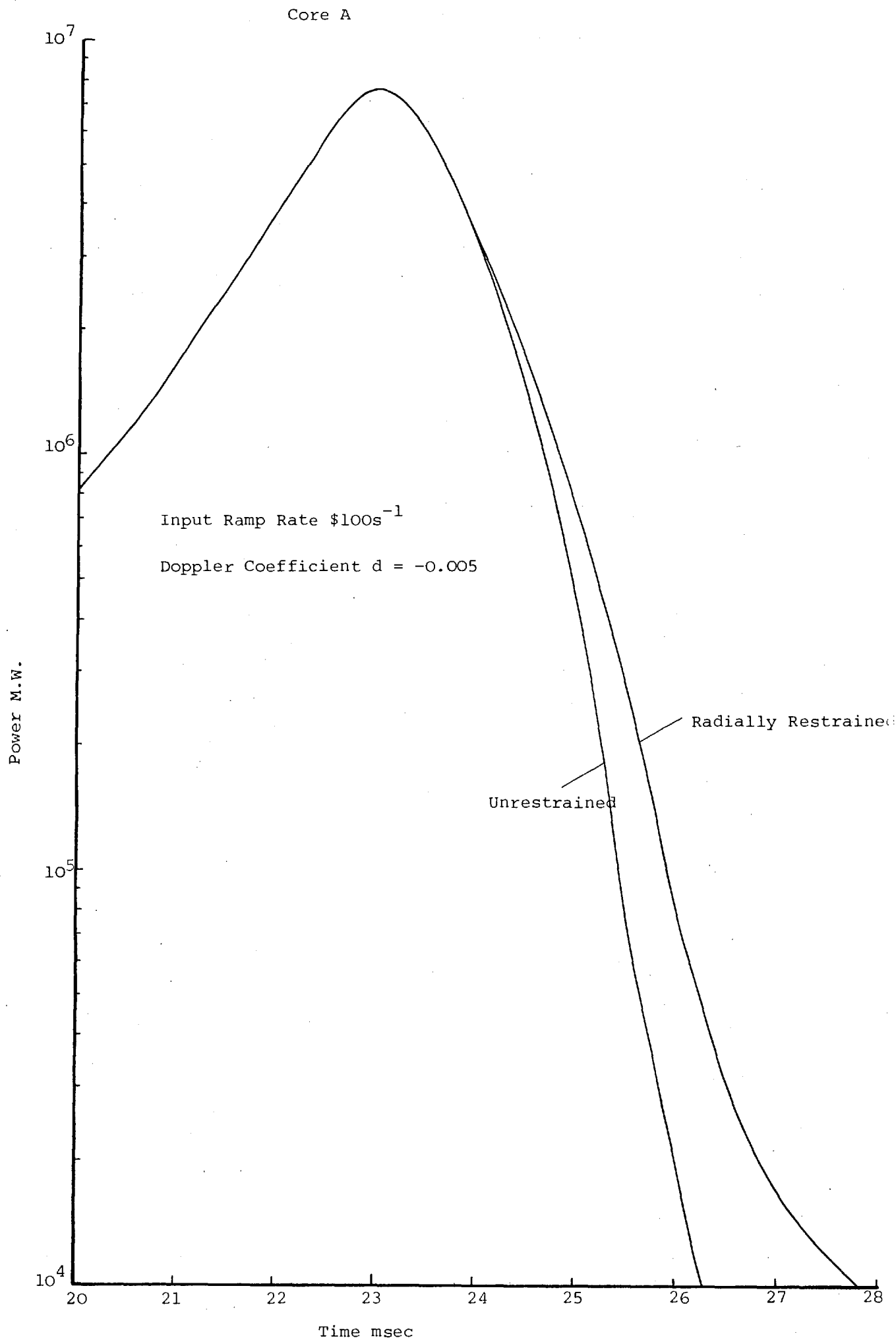


Figure 6.26c Energy History with Radial Restraint -

Core A

Input Ramp Rate 100s^{-1}

Doppler Coefficient $d = -0.005$

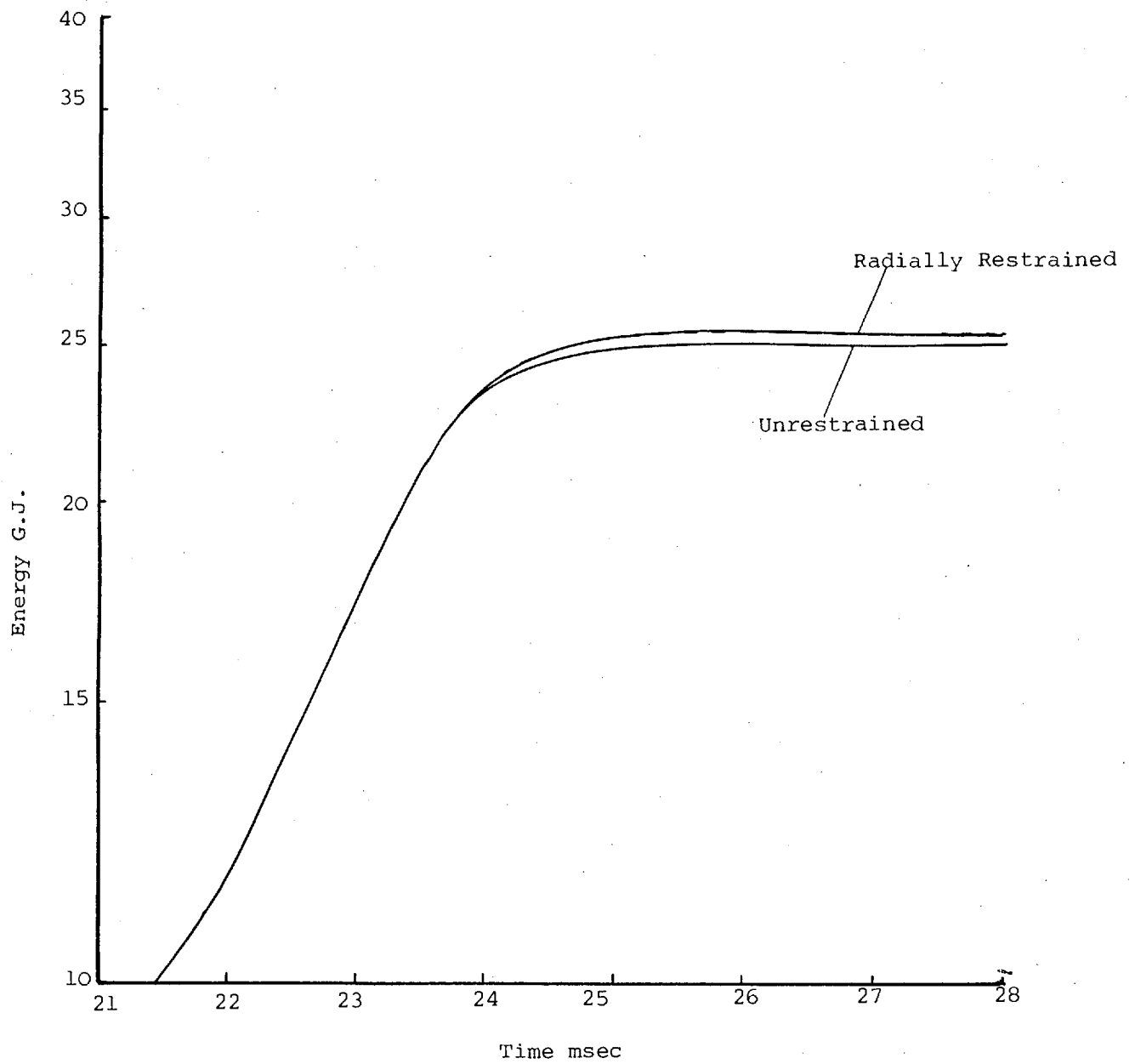


Figure 6.27a Effect of Fission Products and Radial Restraint

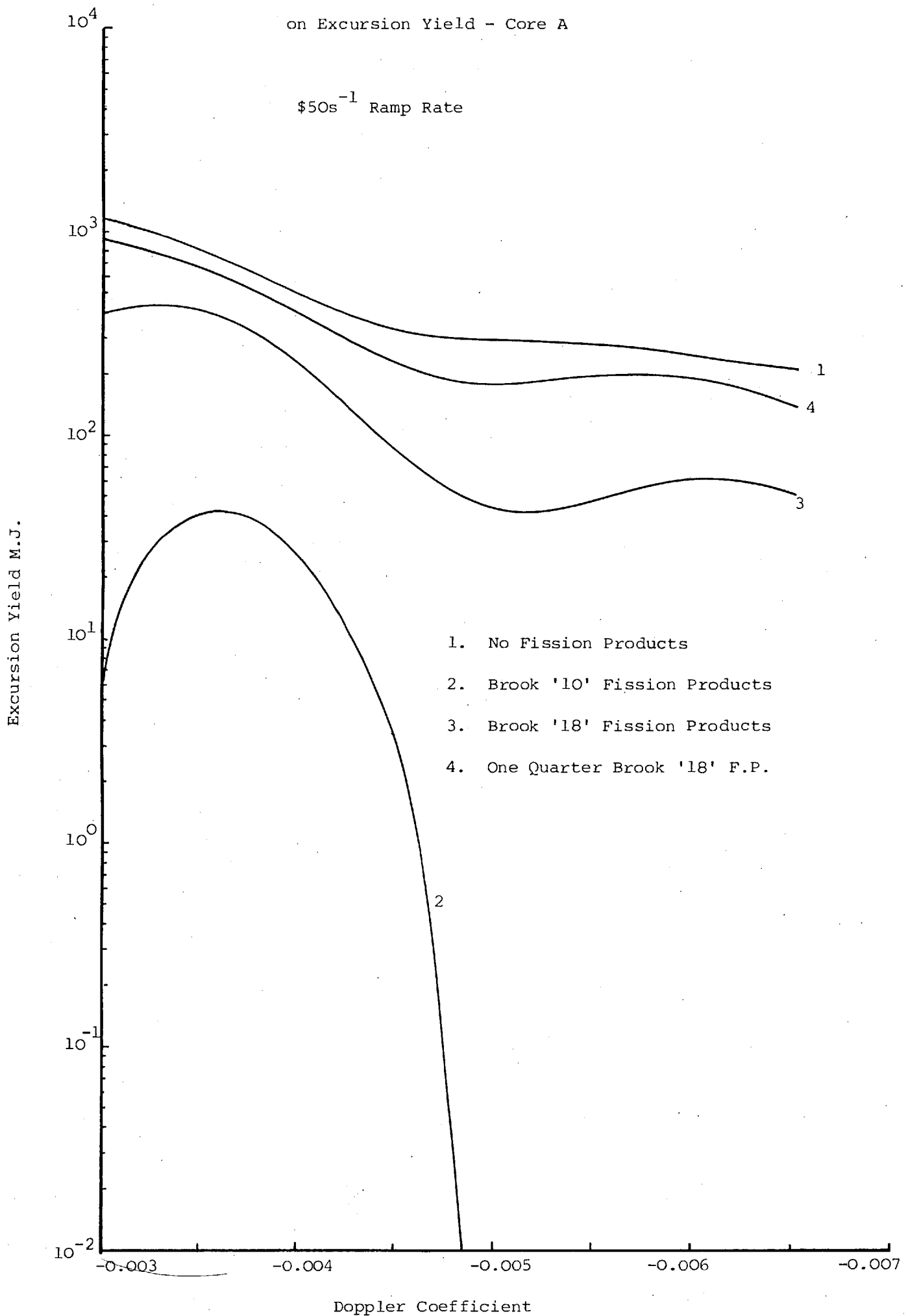


Figure 6.27b Effect of Fission Products and Radial Restraint on

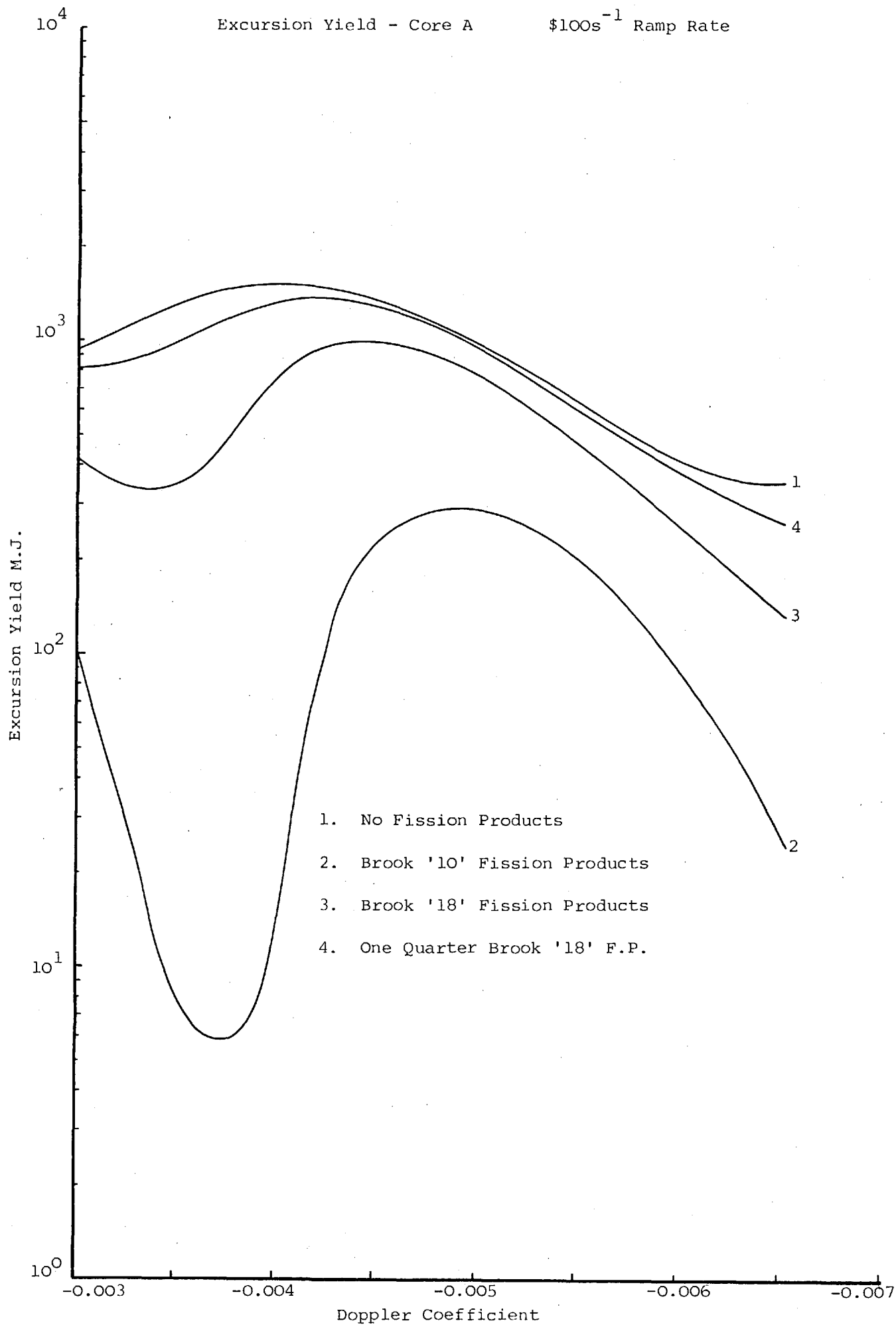


Figure 6.28 Effect of Threshold Pressure on Disassembly

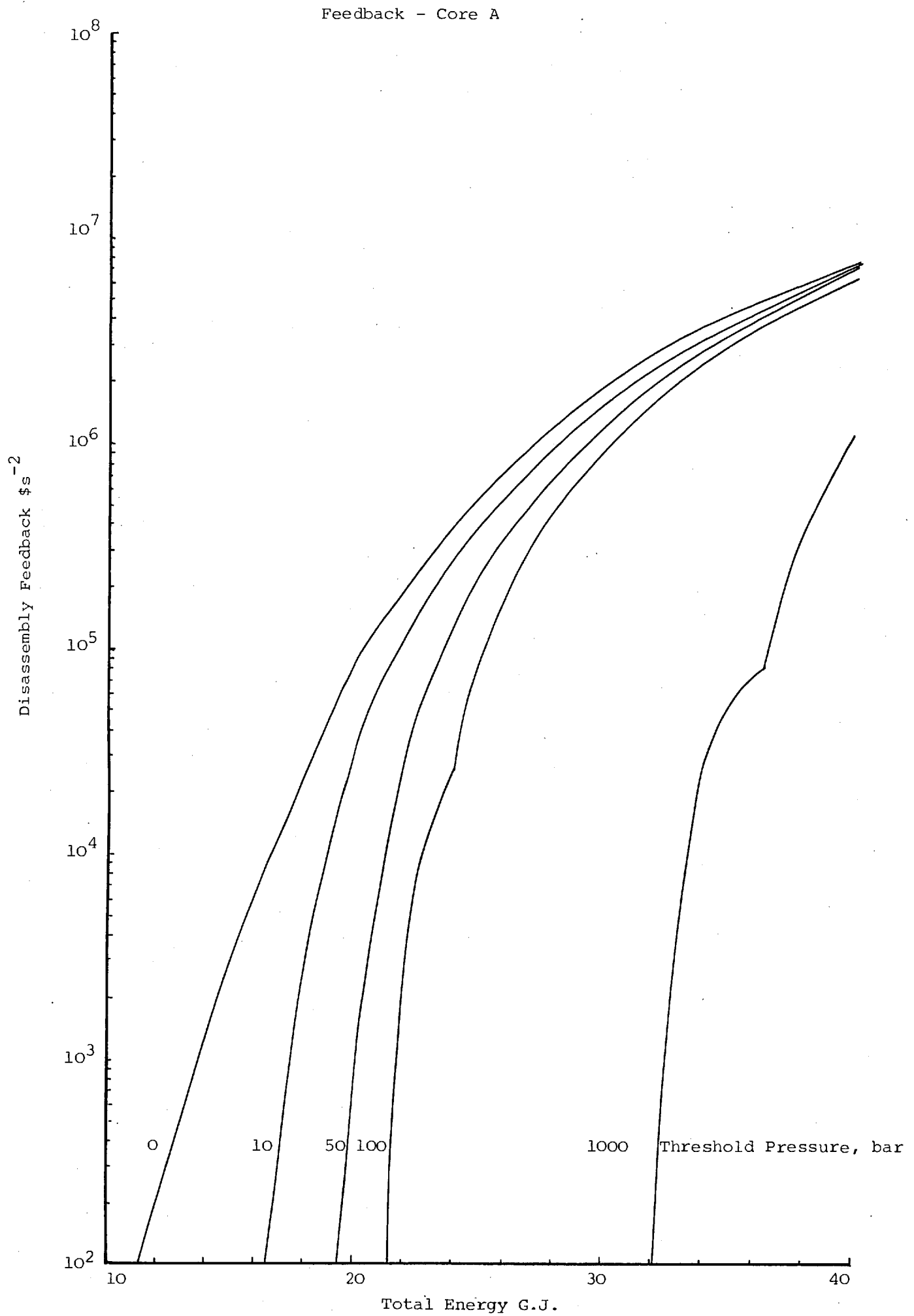
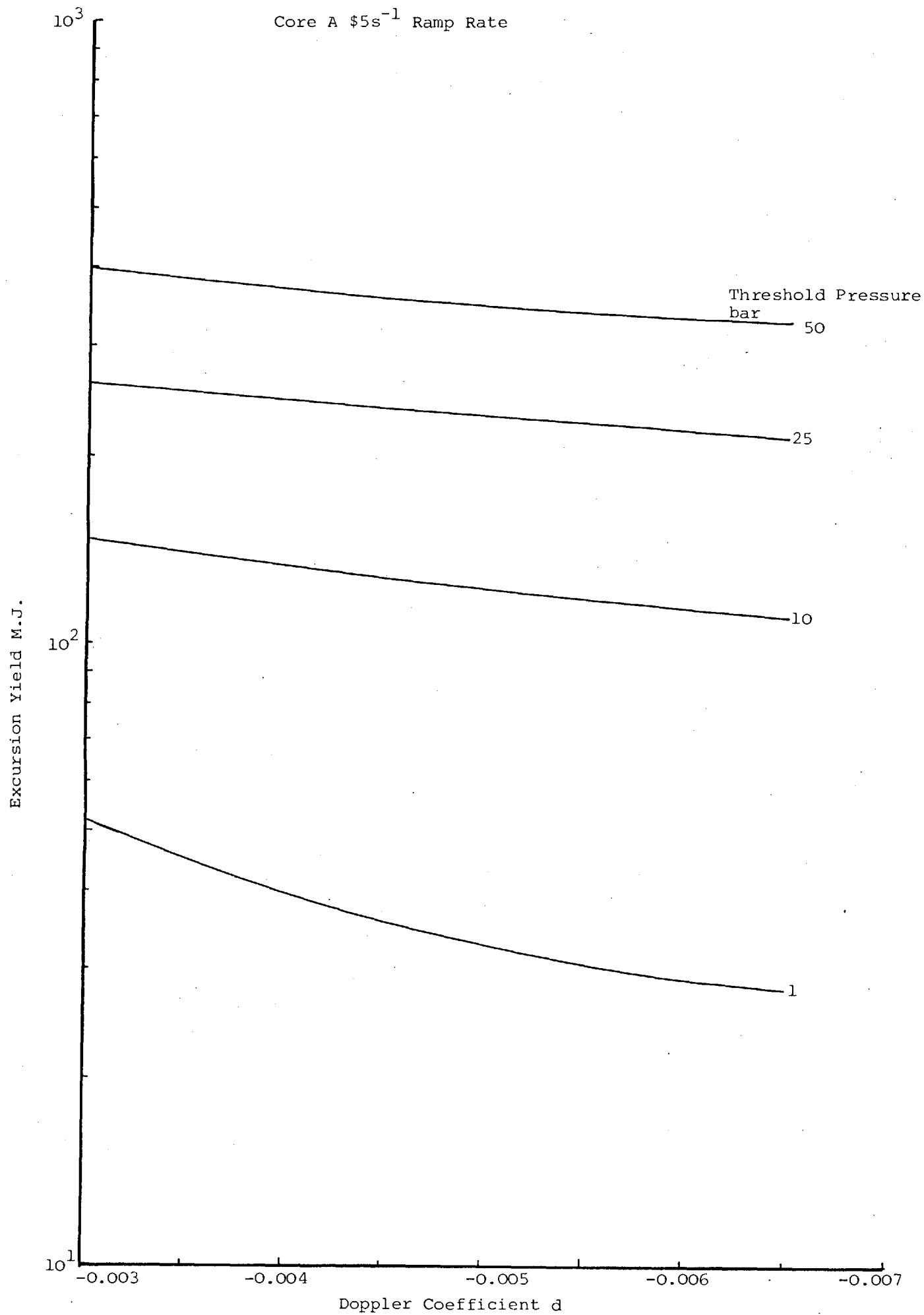


Figure 6.29a Effect of Threshold Pressure on Excursion Yield -



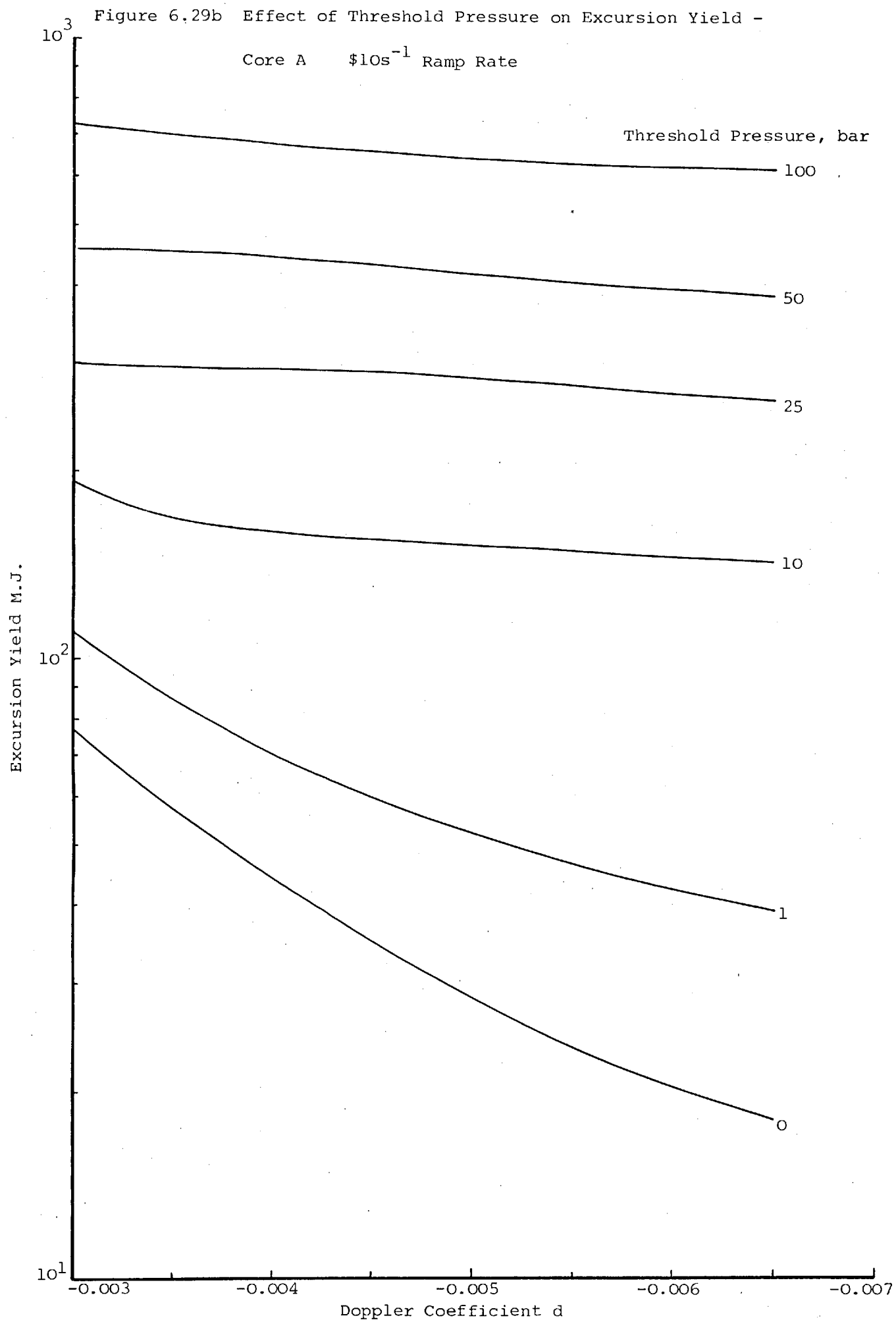


Figure 6.29c Effect of Threshold Pressure on Excursion

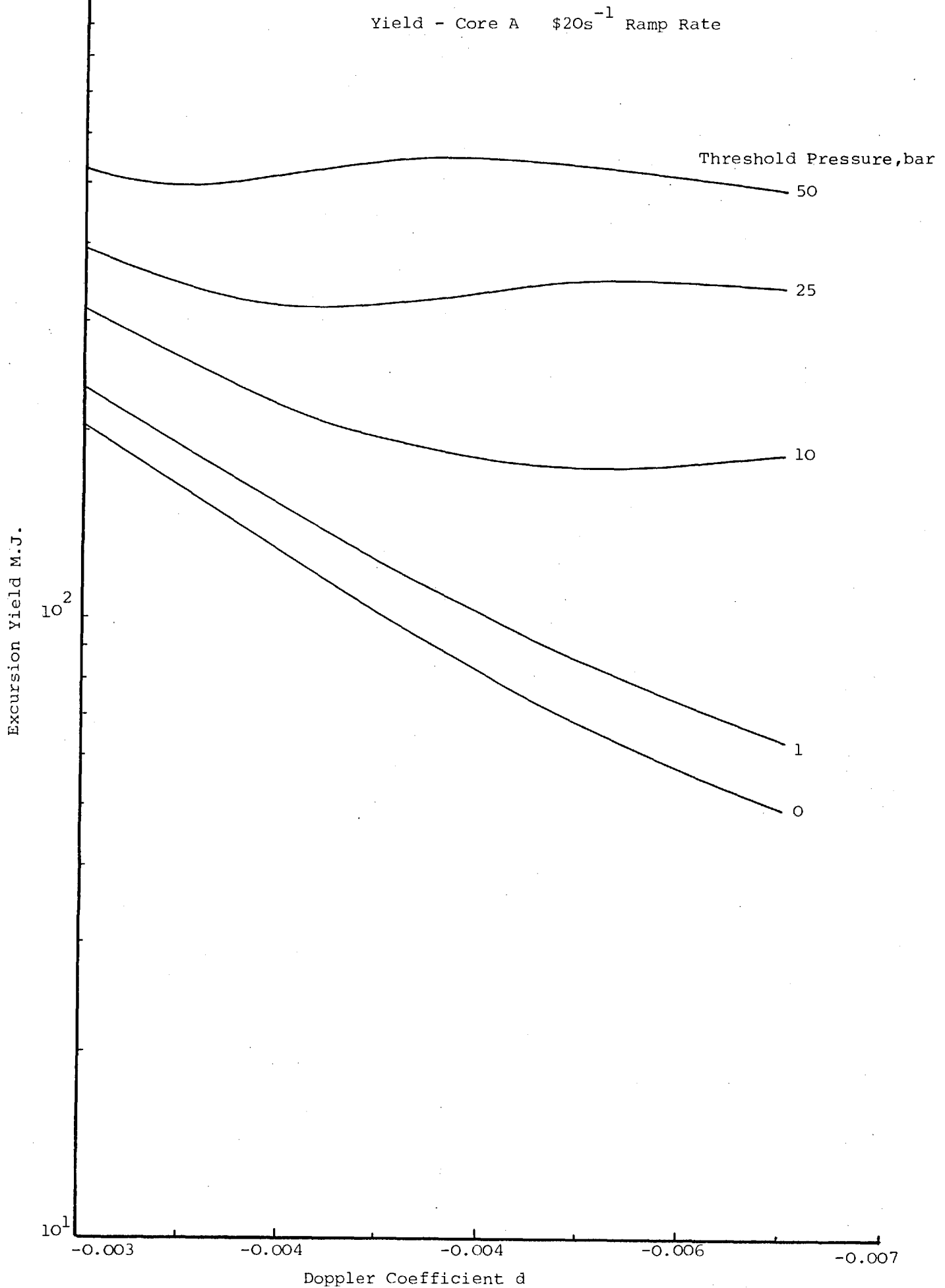


Figure 6.29d Effect of Threshold Pressure on Excursion Yield -

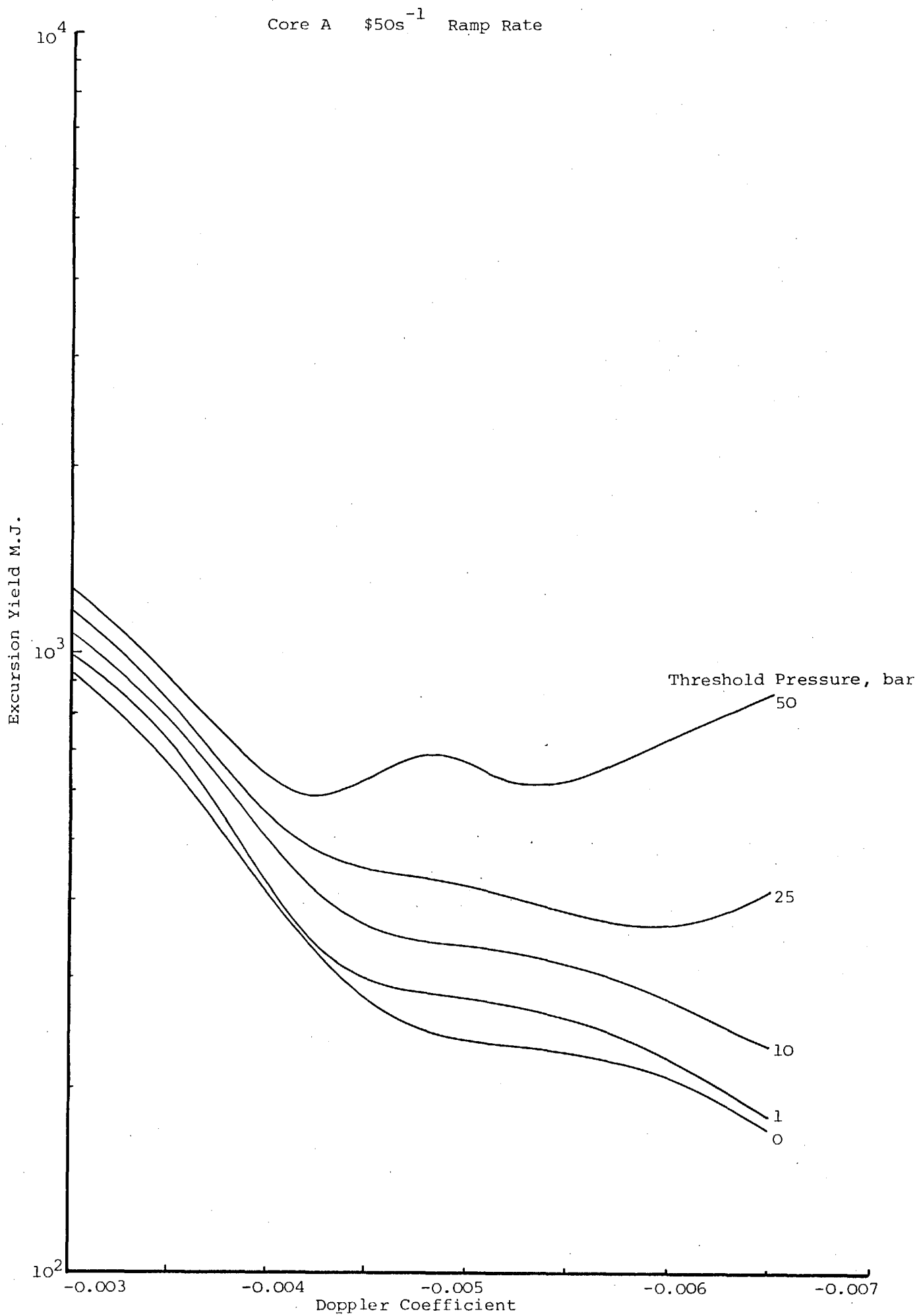


Figure 6.29e Effect of Threshold Pressure on Excursion

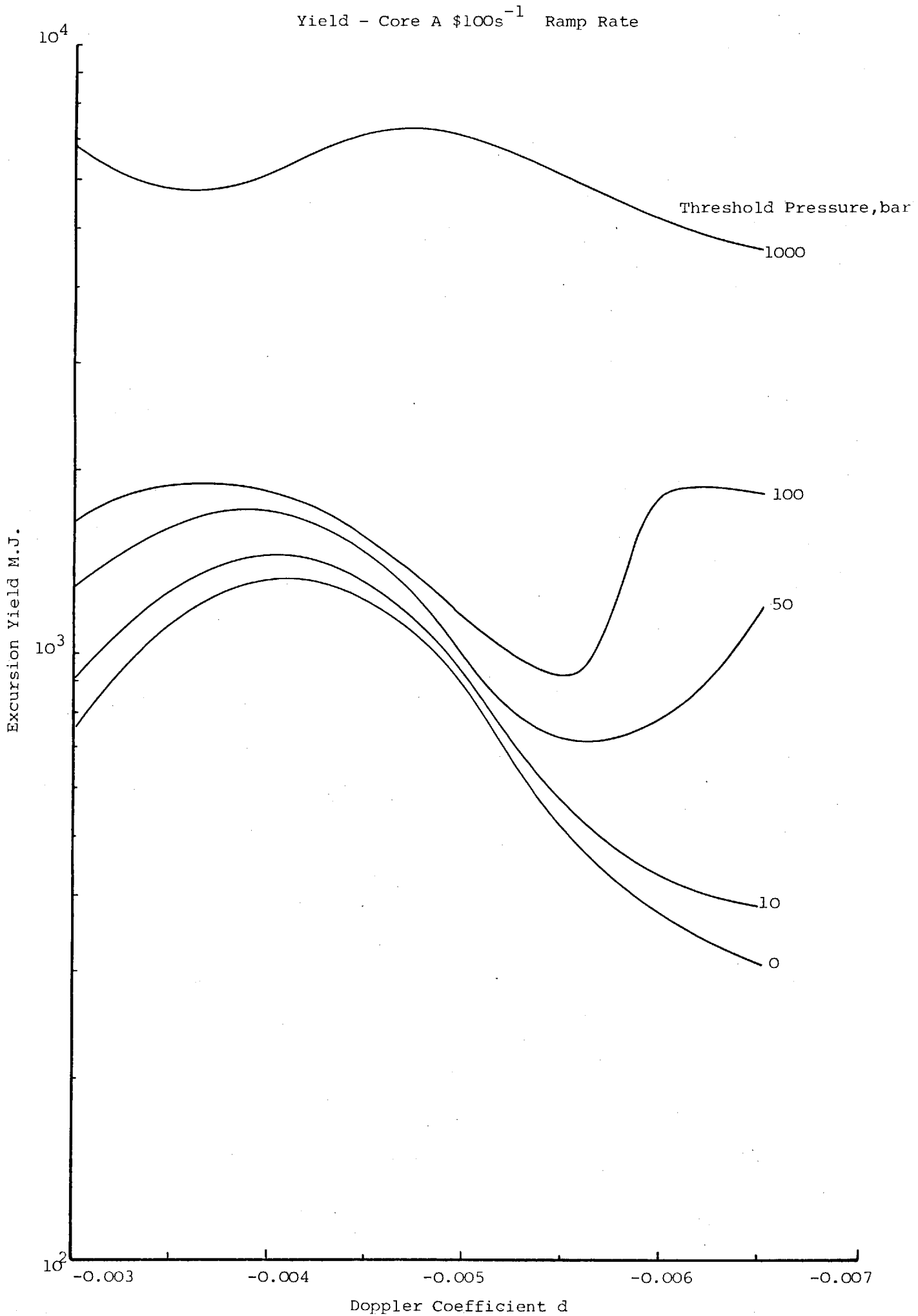


Figure 6.30 Reactivity History with Threshold Pressure -

- 220

Core A

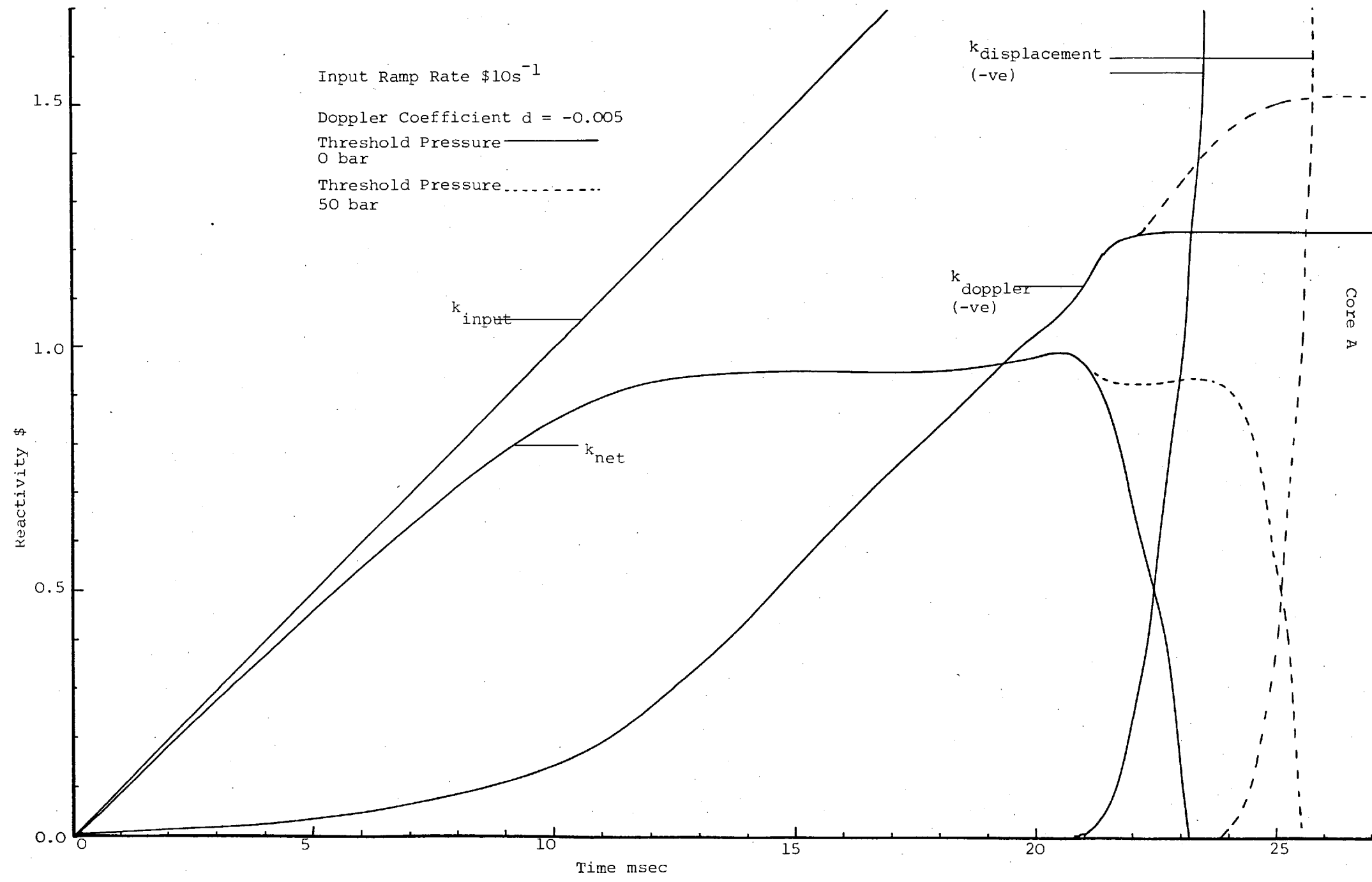


Figure 6.31 Effect of Threshold Pressure on Excursion Yield

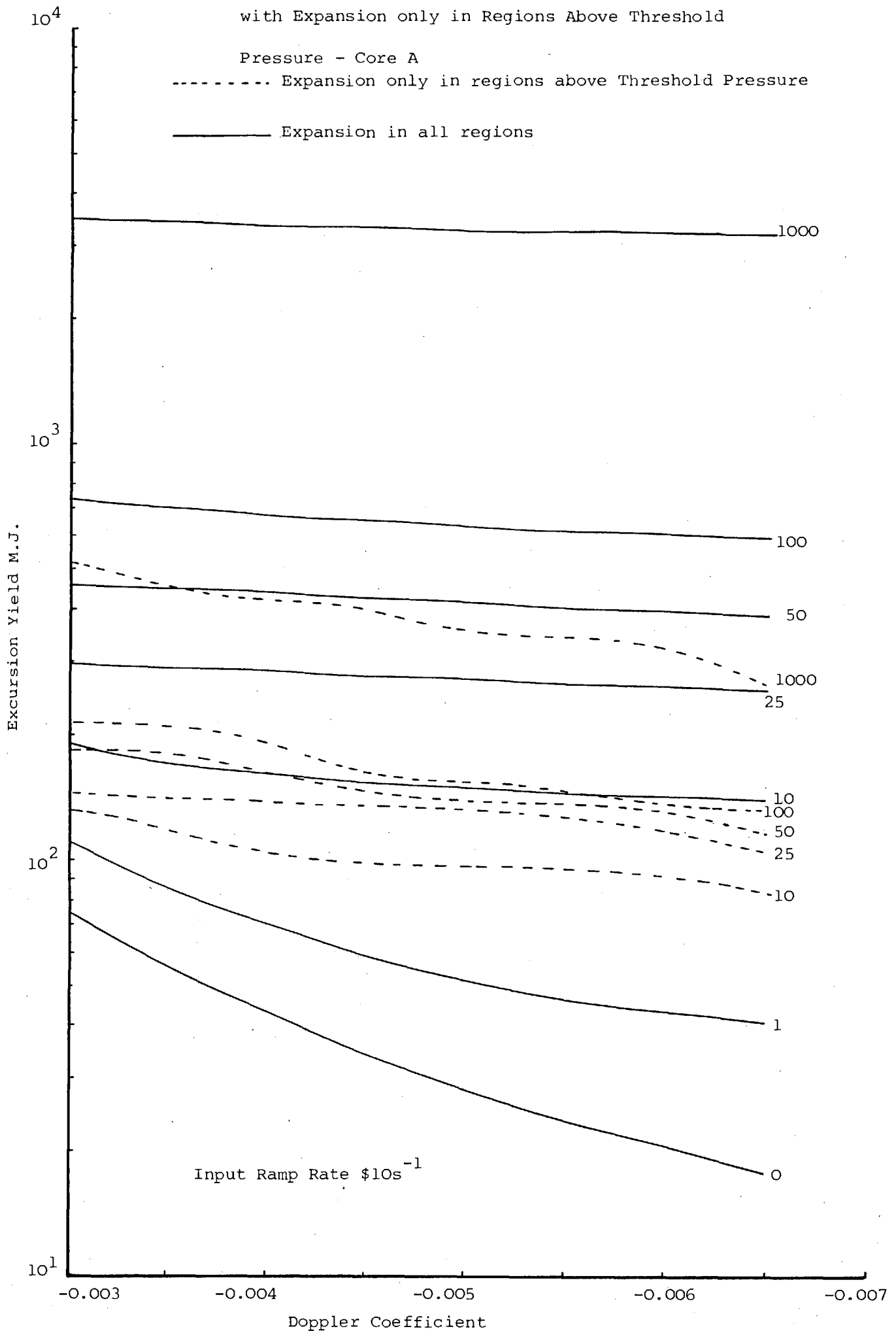


Figure 6.32 Effect of Threshold Pressure and Fission Products on
Excursion Yield - Core A, 100s^{-1} Ramp Rate, Brook '10'

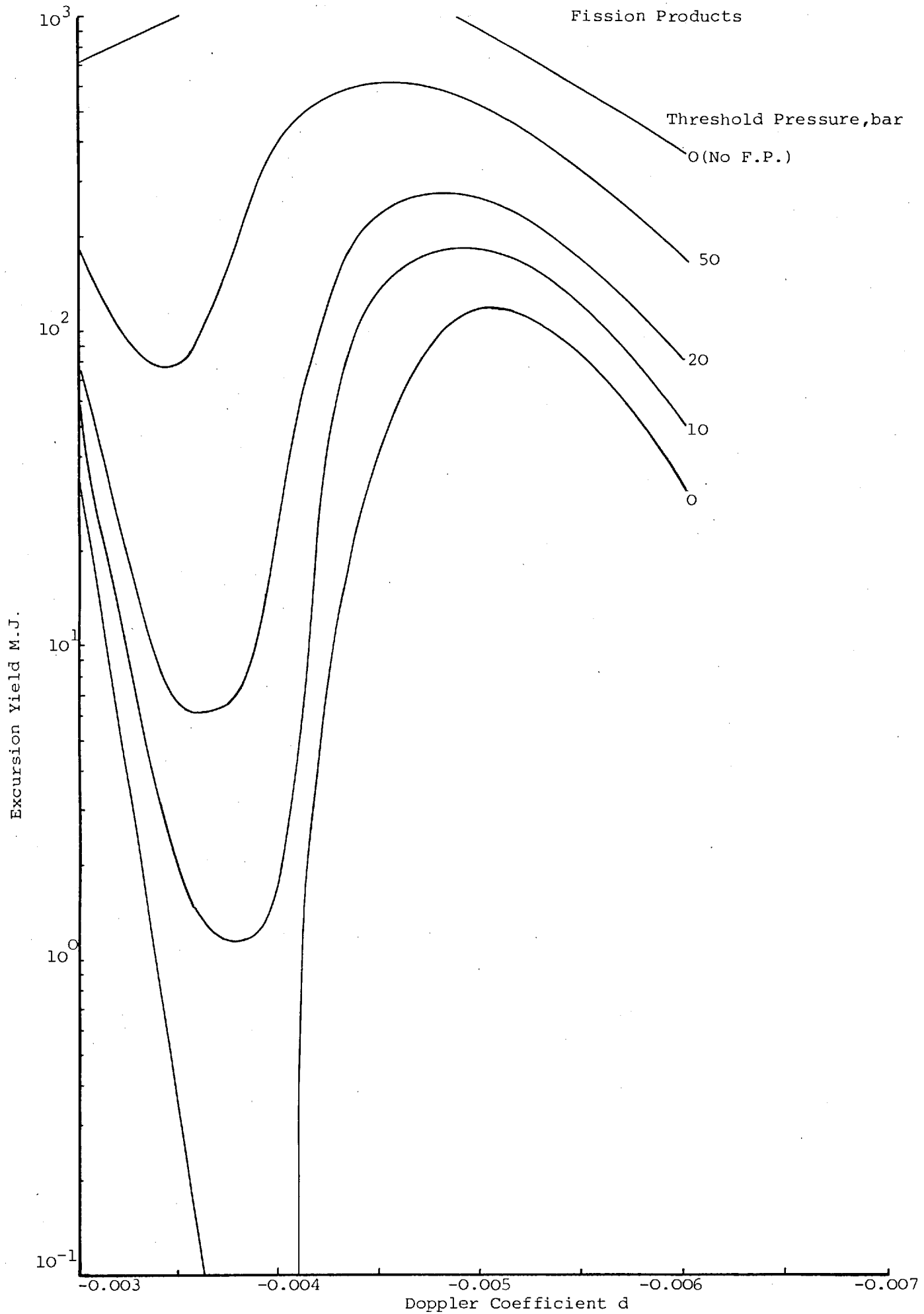


Figure 6.33 Effect of Initial Temperature Variation on Mass

Weighted Average Temperature - Core B

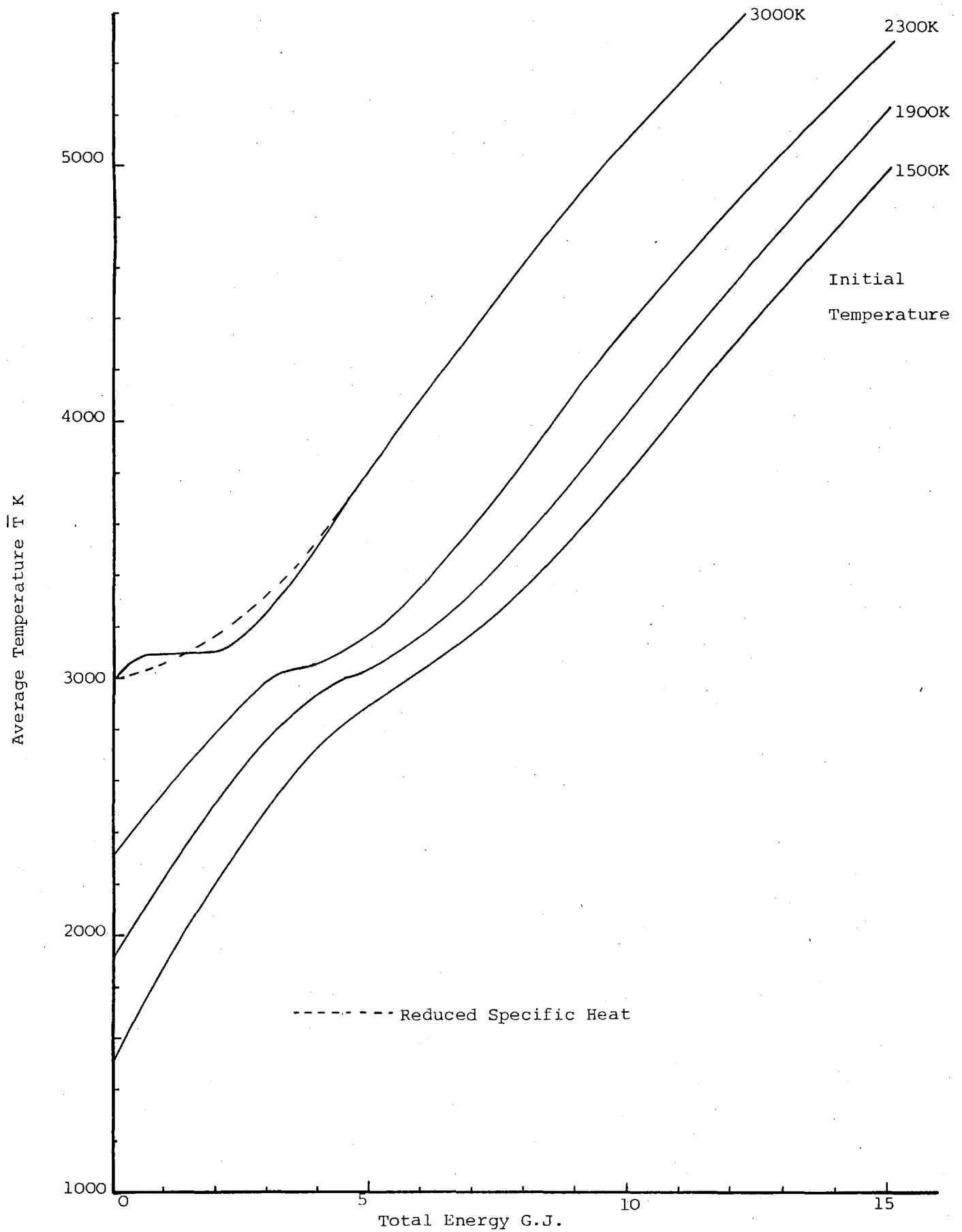


Figure 6.34a Effect of Initial-Temperature Variation
on Doppler Feedback - Core B

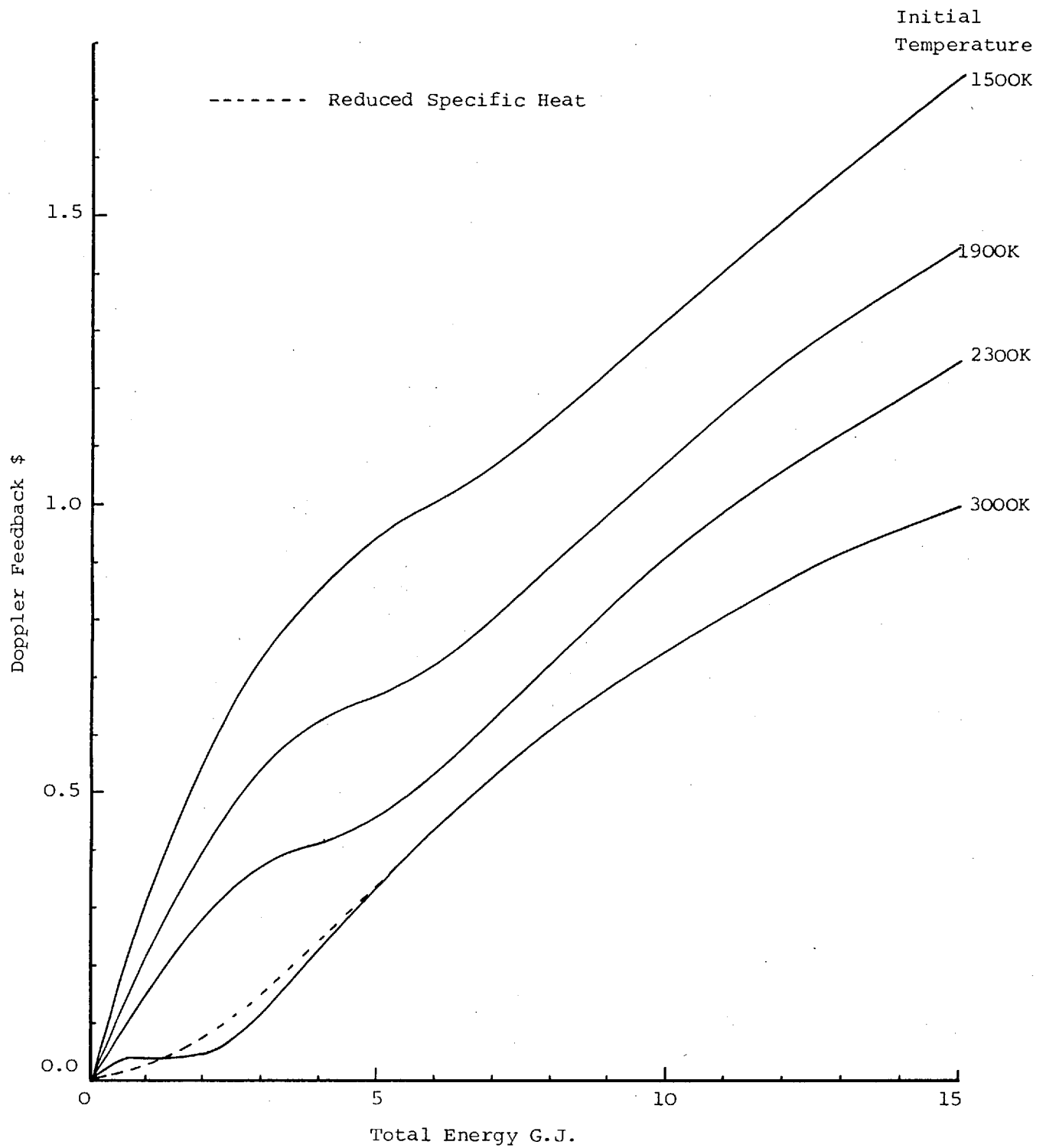


Figure 6.34b Effect of Initial Temperature Variation on

Disassembly Feedback - Core B

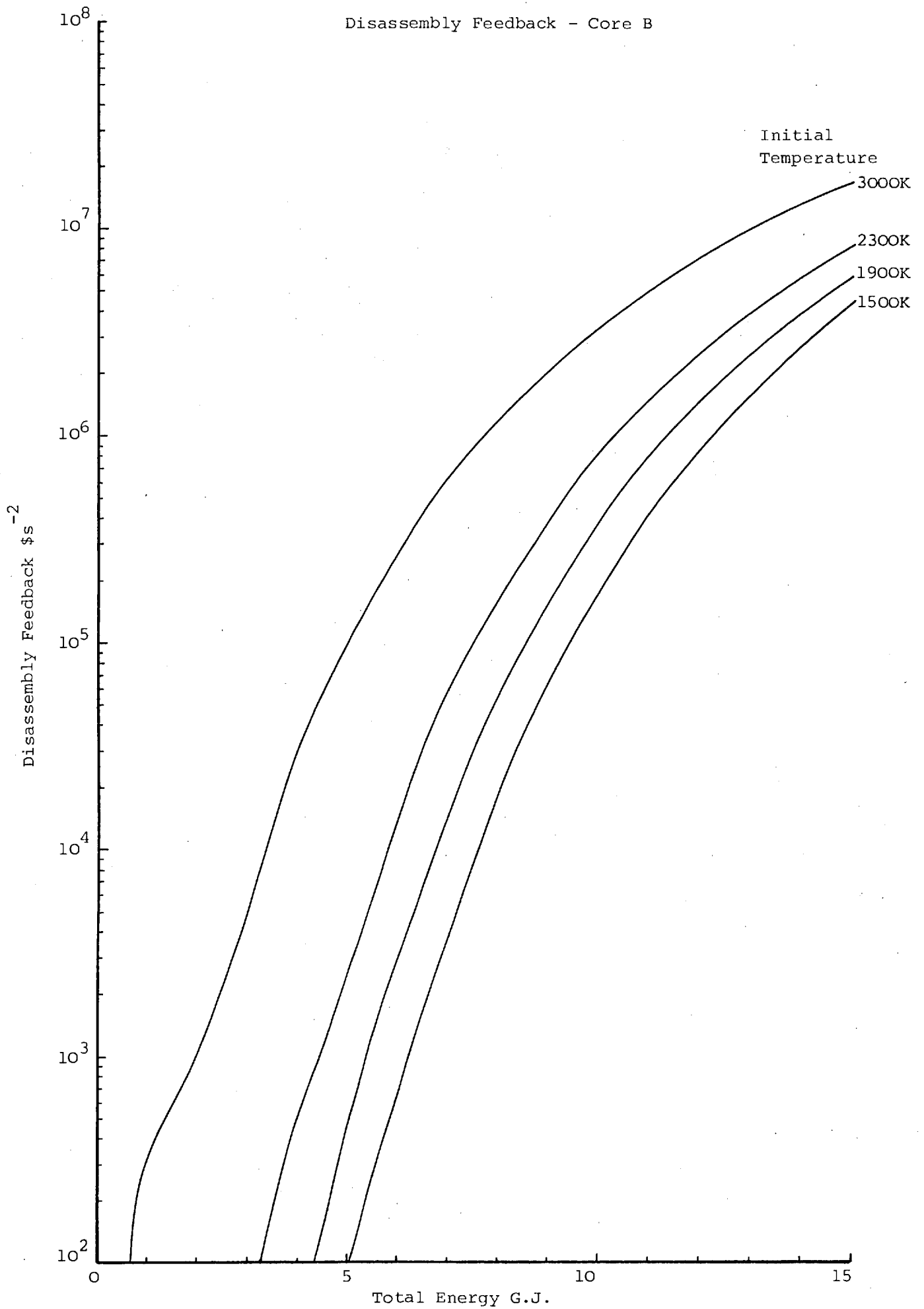


Figure 6.35a Effect of Initial Temperature Variation on

Excursion Yield - Core B, 10s^{-1} Ramp Rate

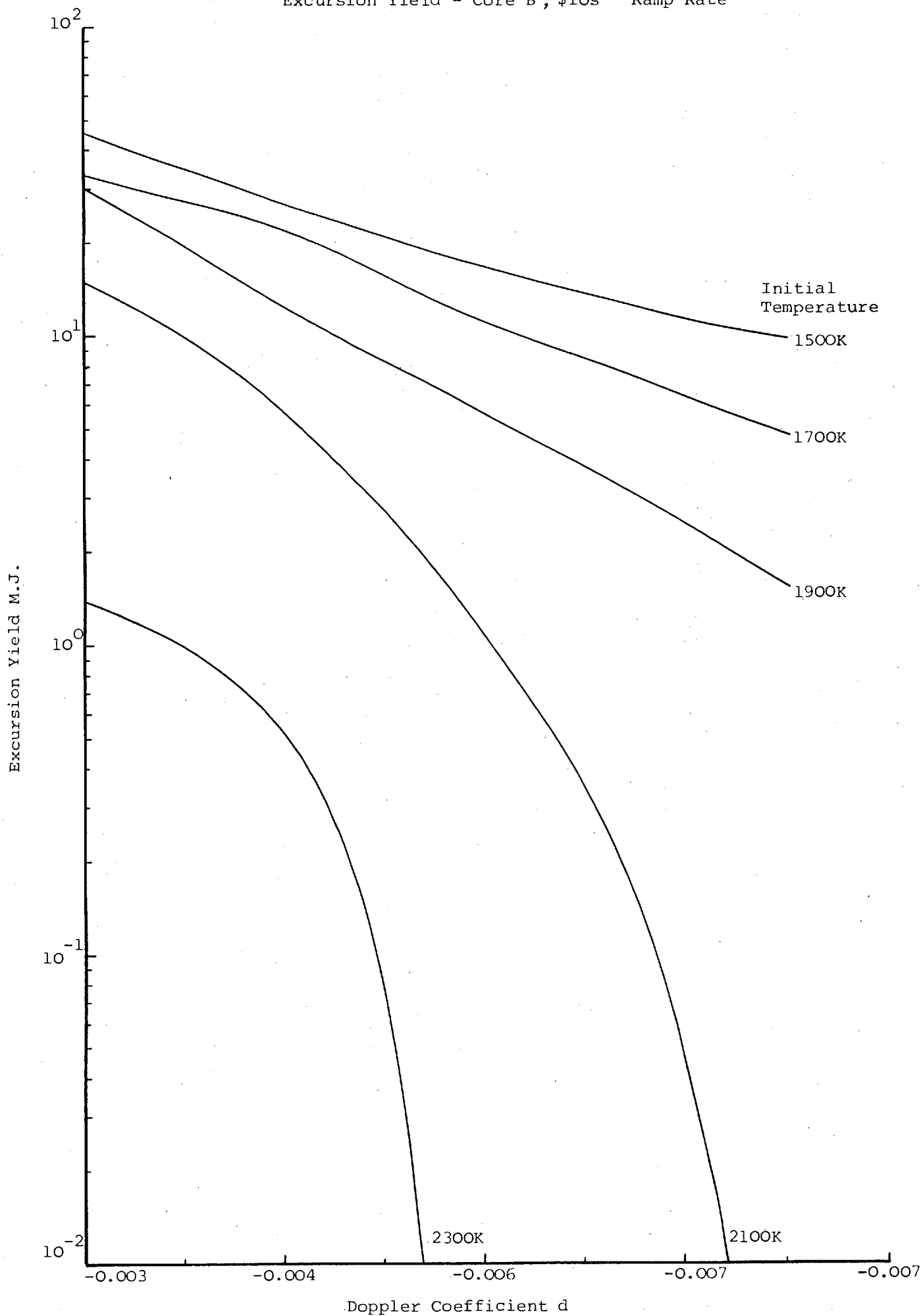


Figure 6.35b Effect of Initial Temperature Variation on

Excursion Yield - Core B, $50s^{-1}$ Ramp Rate

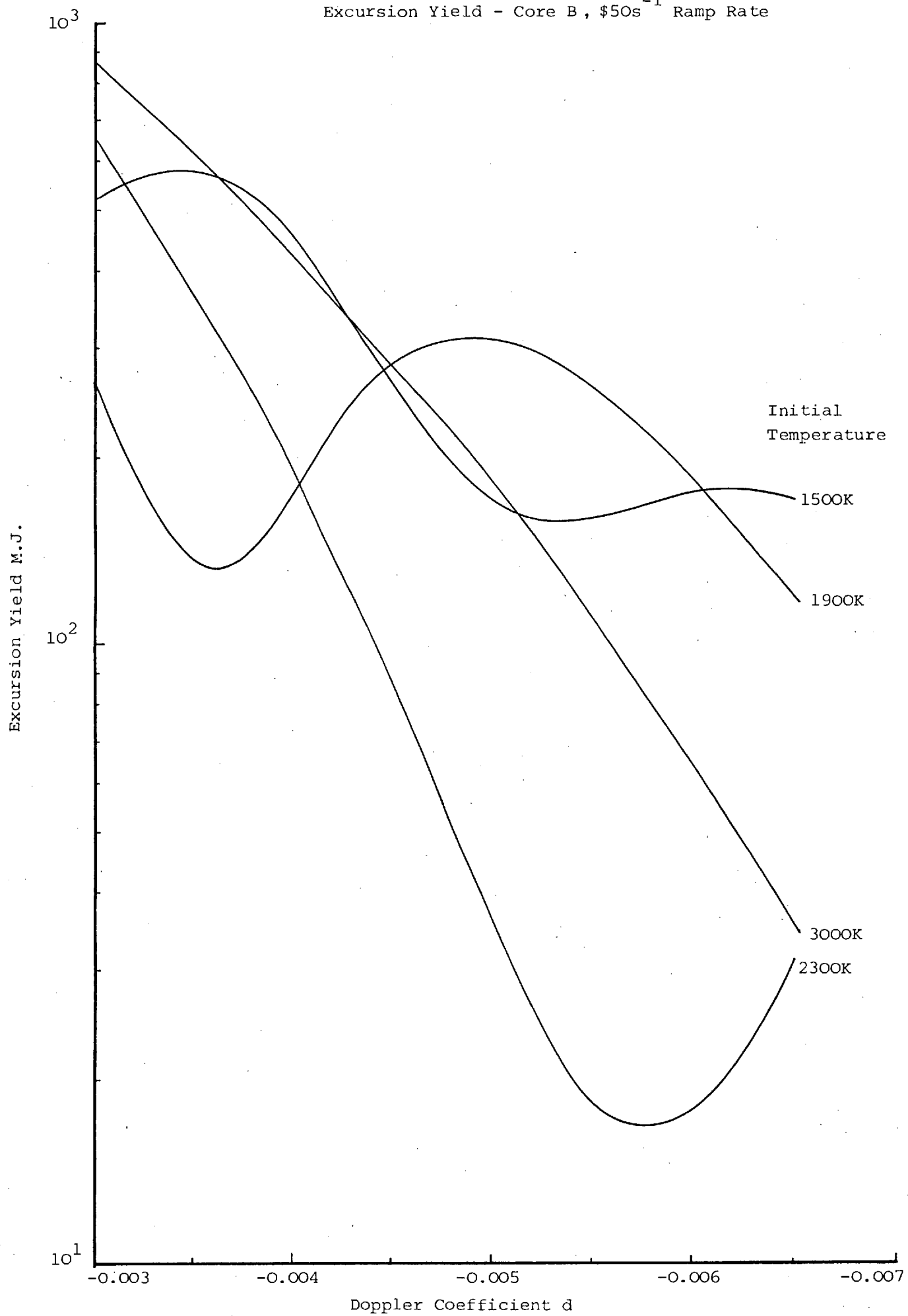


Figure 6.35d Effect of Initial Temperature Variation of
Excursion Yield - Core B, 500s^{-1} Ramp Rate

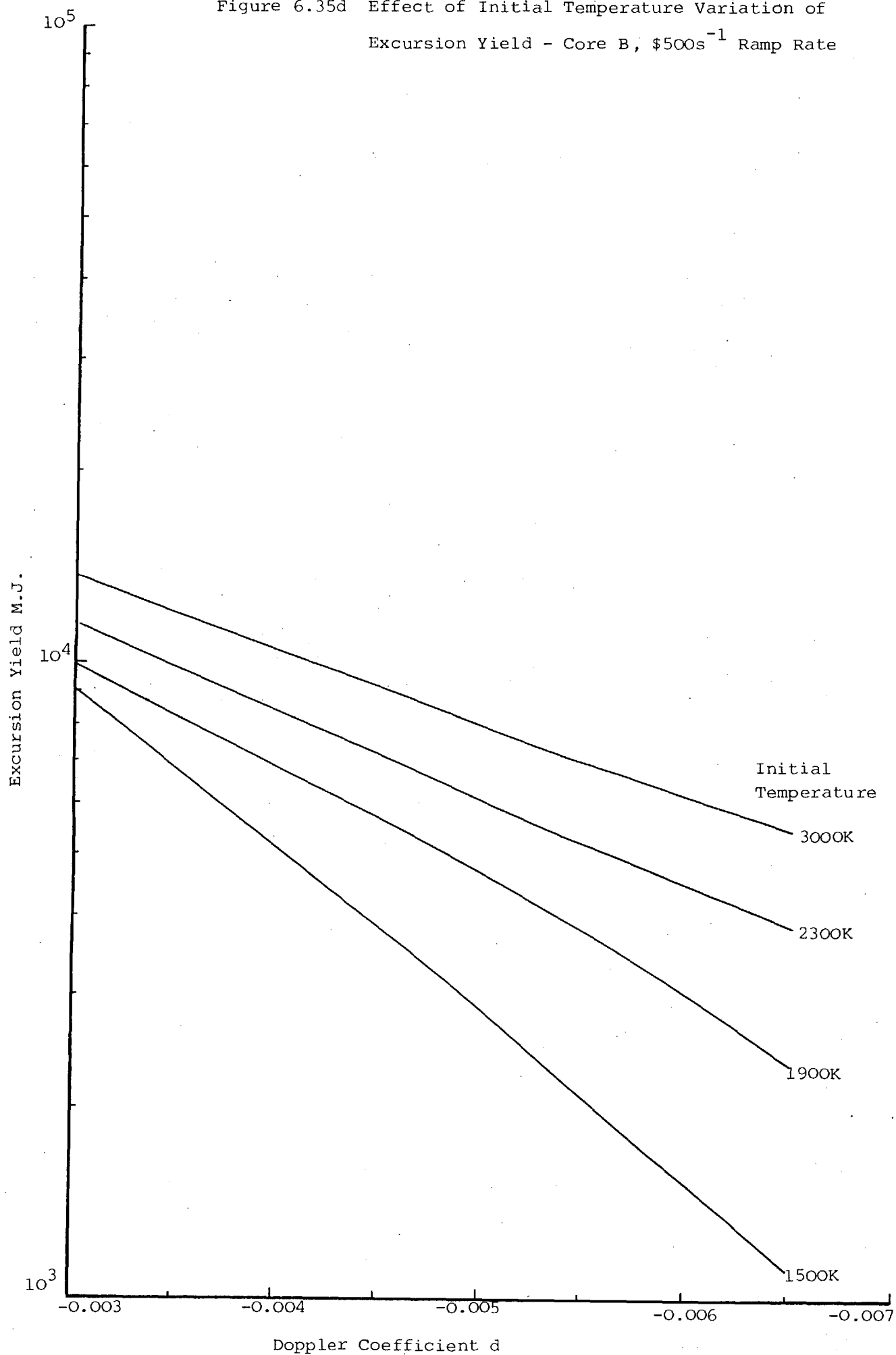


Figure 6.36a Effect of Initial Power on Power History

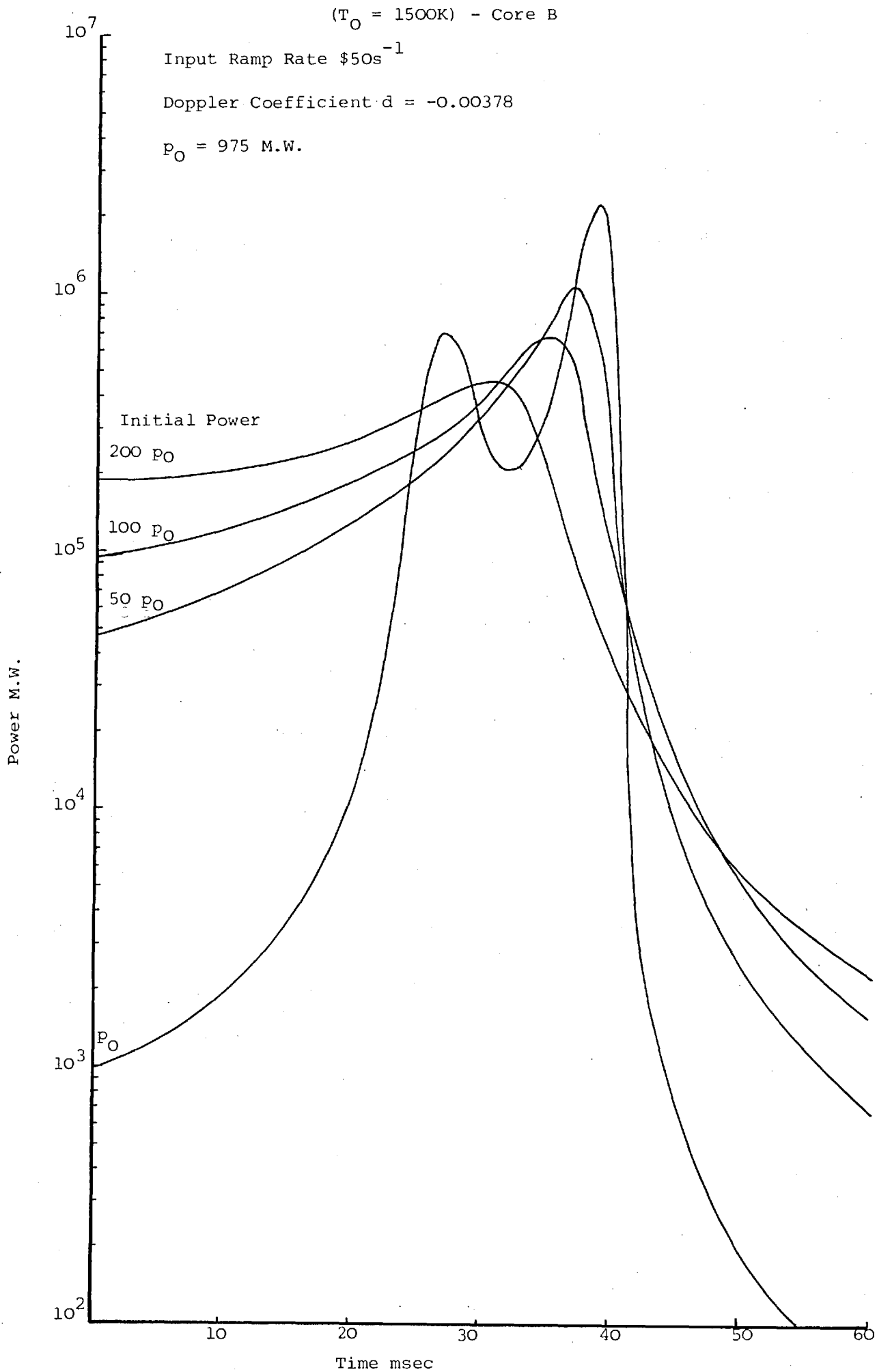


Figure 6.36b Effect of Initial Power on Power History

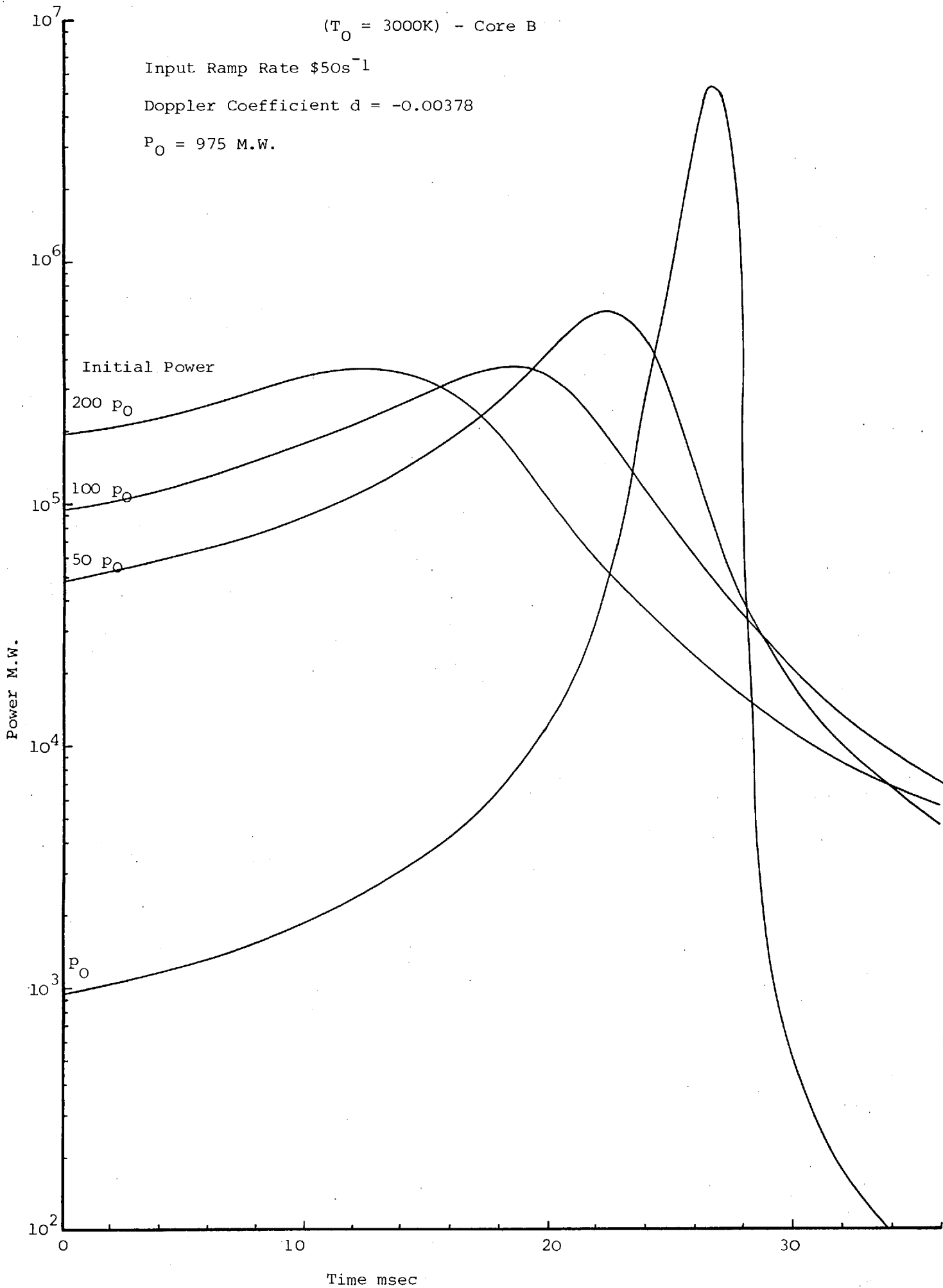


Figure 6.37a Effect of Initial Reactivity on Power History

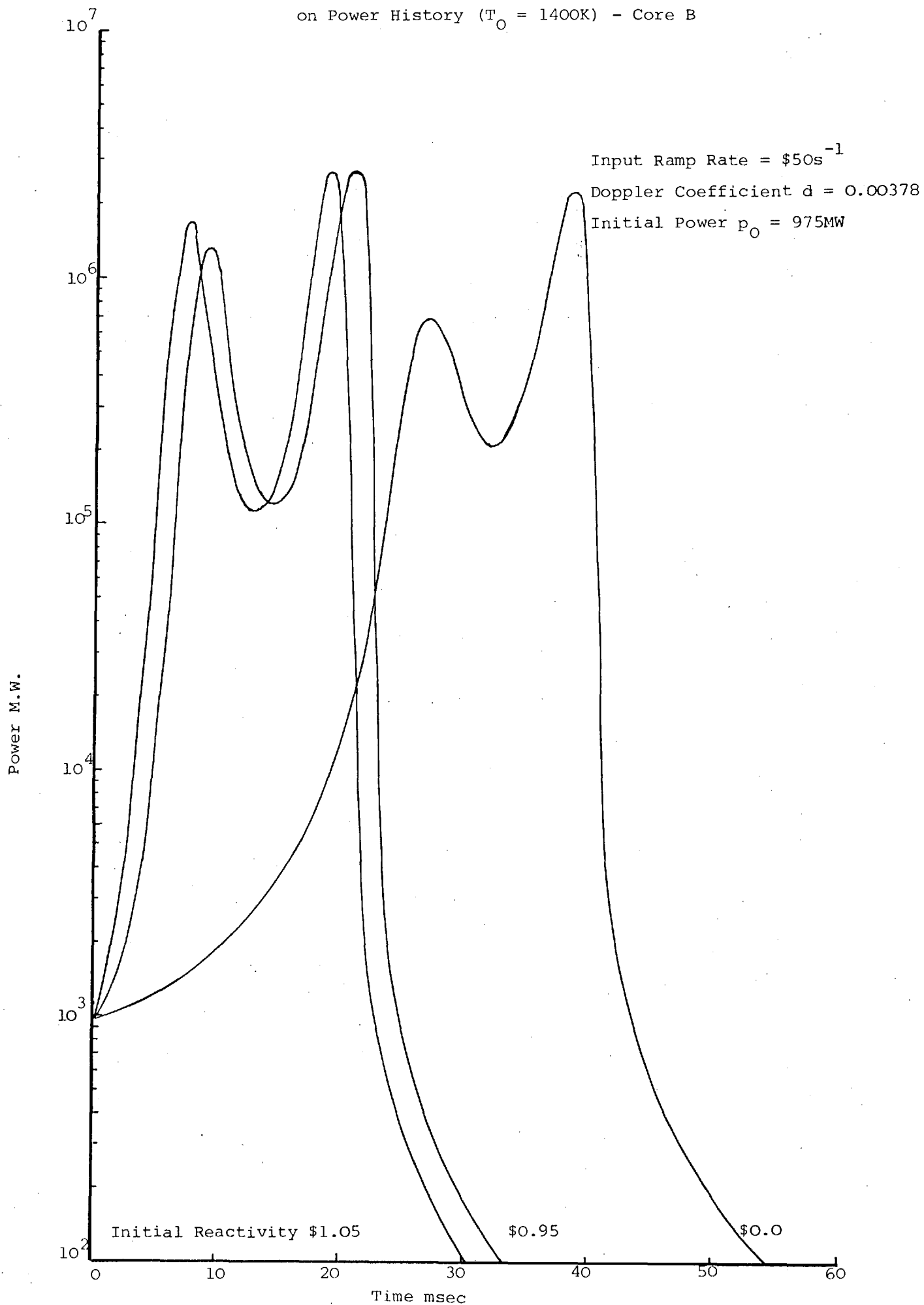


Figure 6.37b Effect of Initial Reactivity on Power History

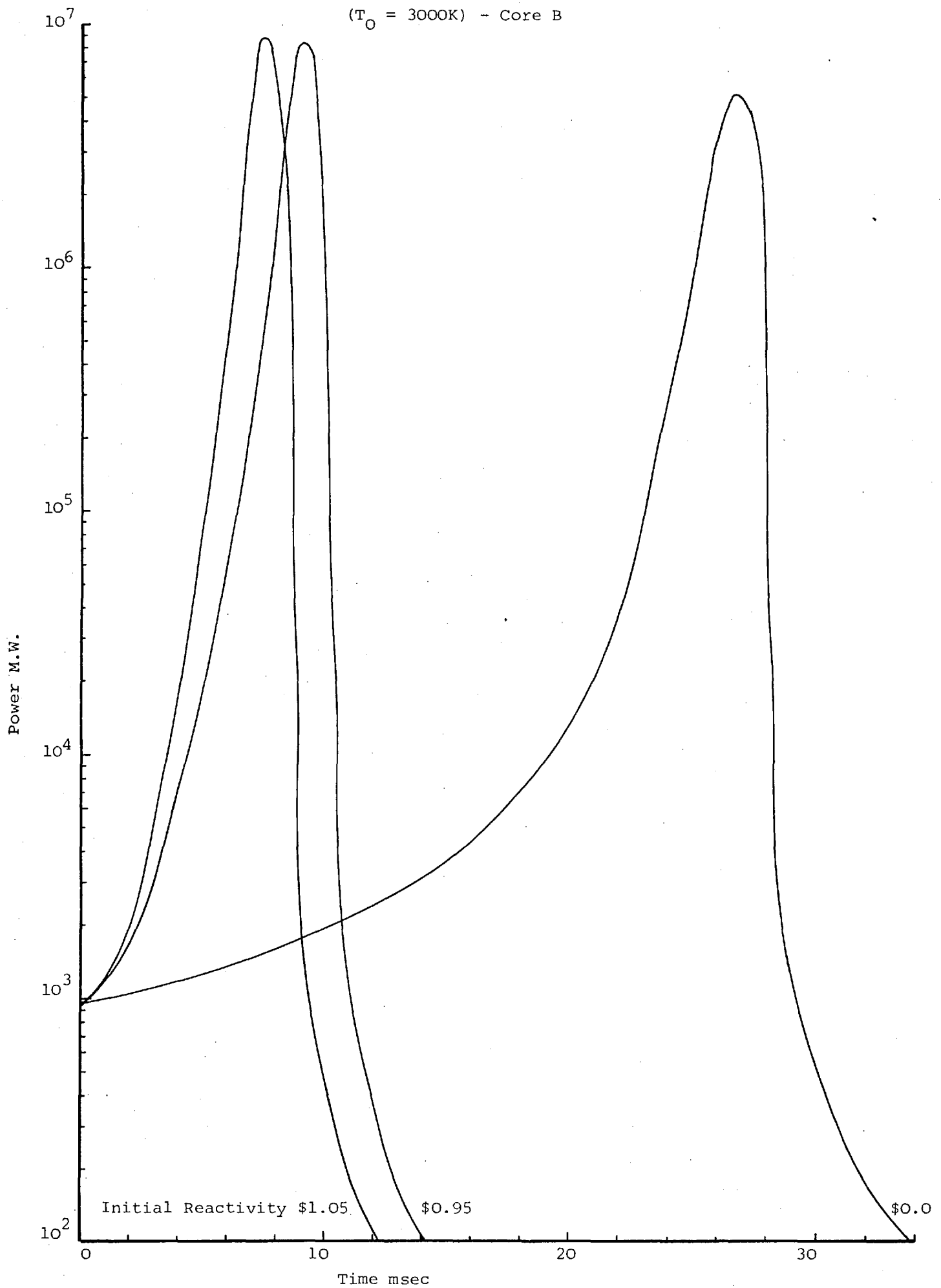


Figure 6.38 Variation of Yield with Initial Power, Reactivity

and Precursor Density

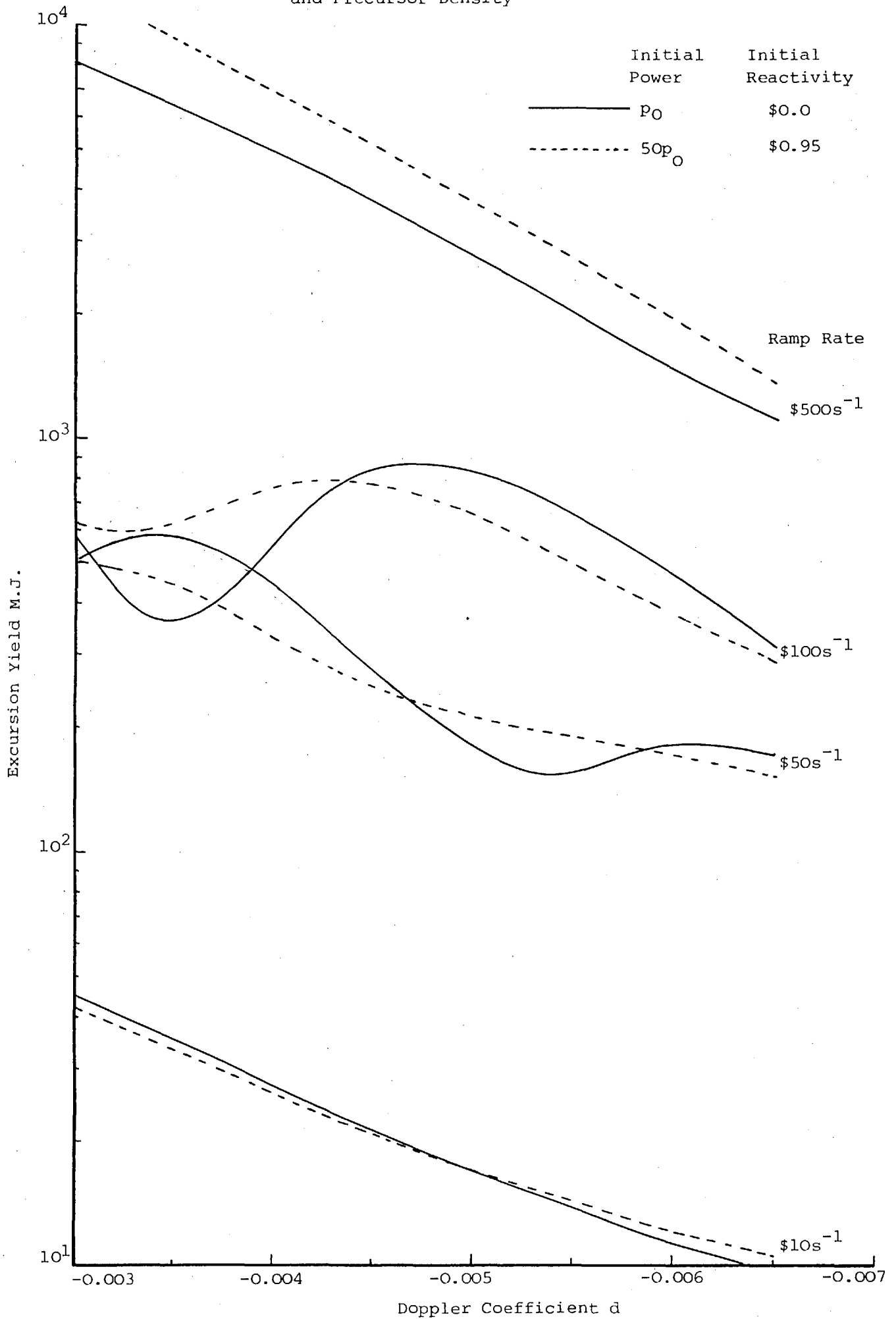


Figure 6.39a Comparison of VENUS-II and EXTRA Power Histories

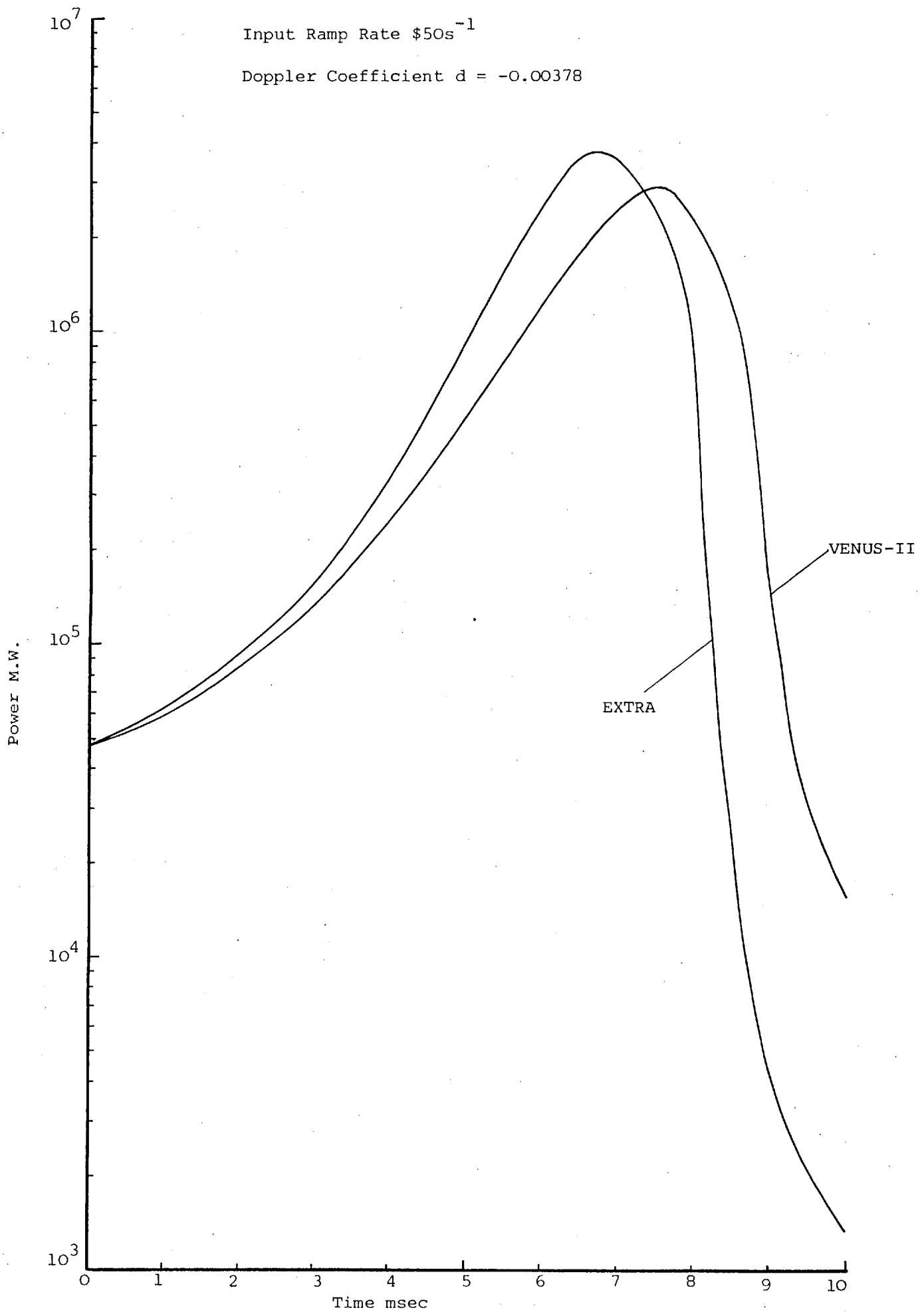
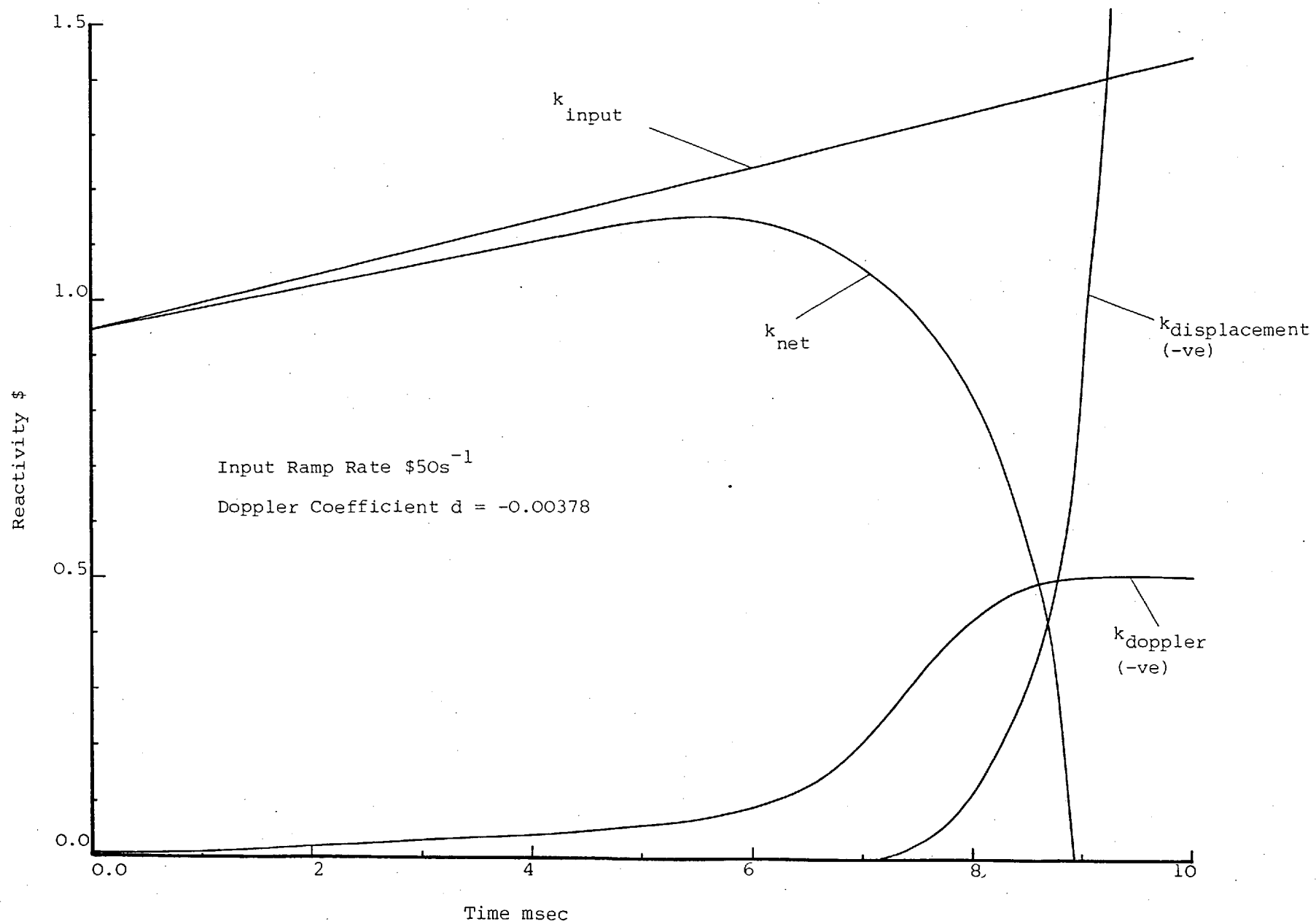


Figure 6.39b VENUS-II Reactivity History for Comparison
Case (after 37)



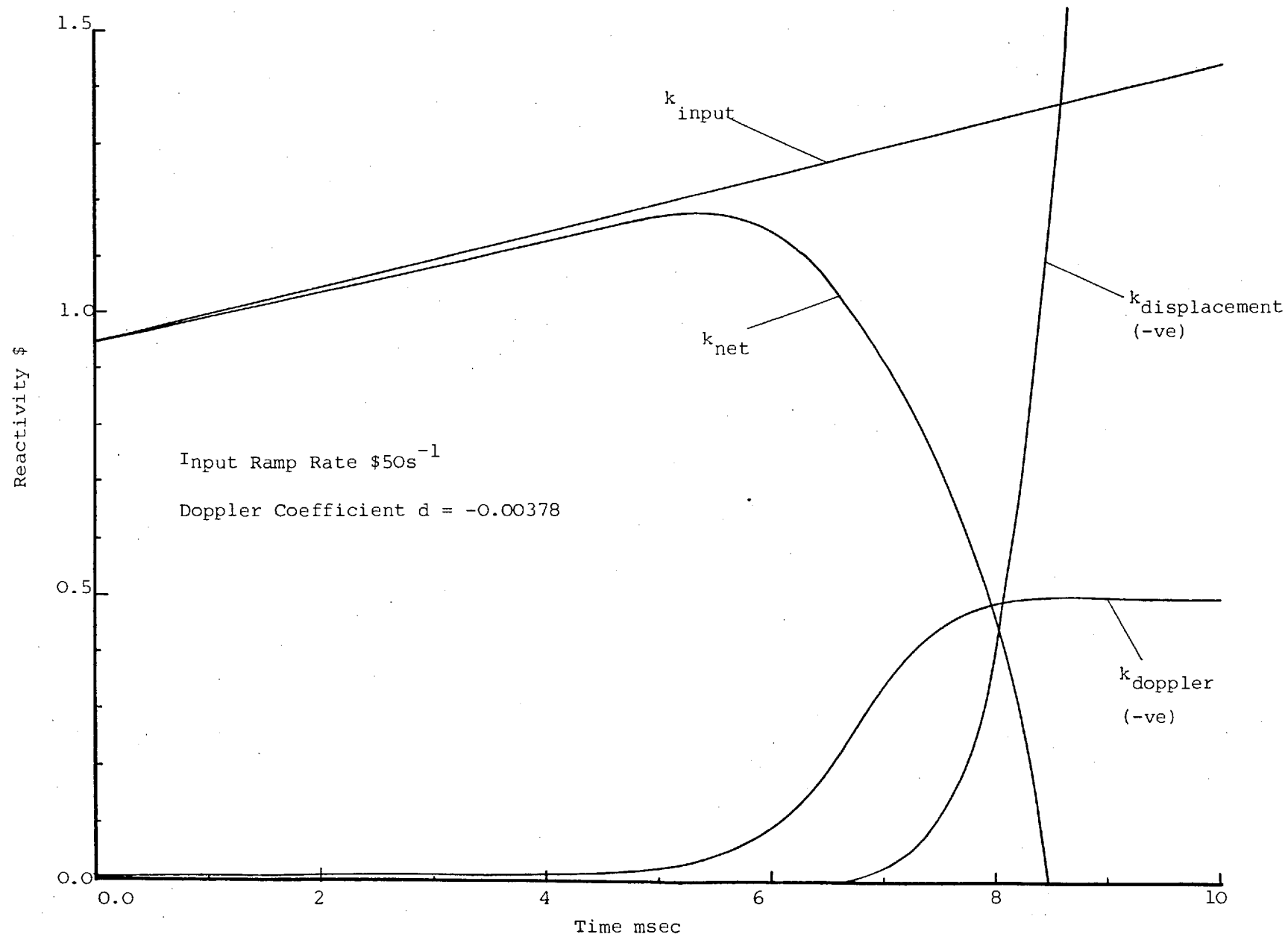


Figure 6.39c EXTRA Reactivity History for Comparison Case

Figure 6.40a Effect of Final Pressure on Excursion Yield -

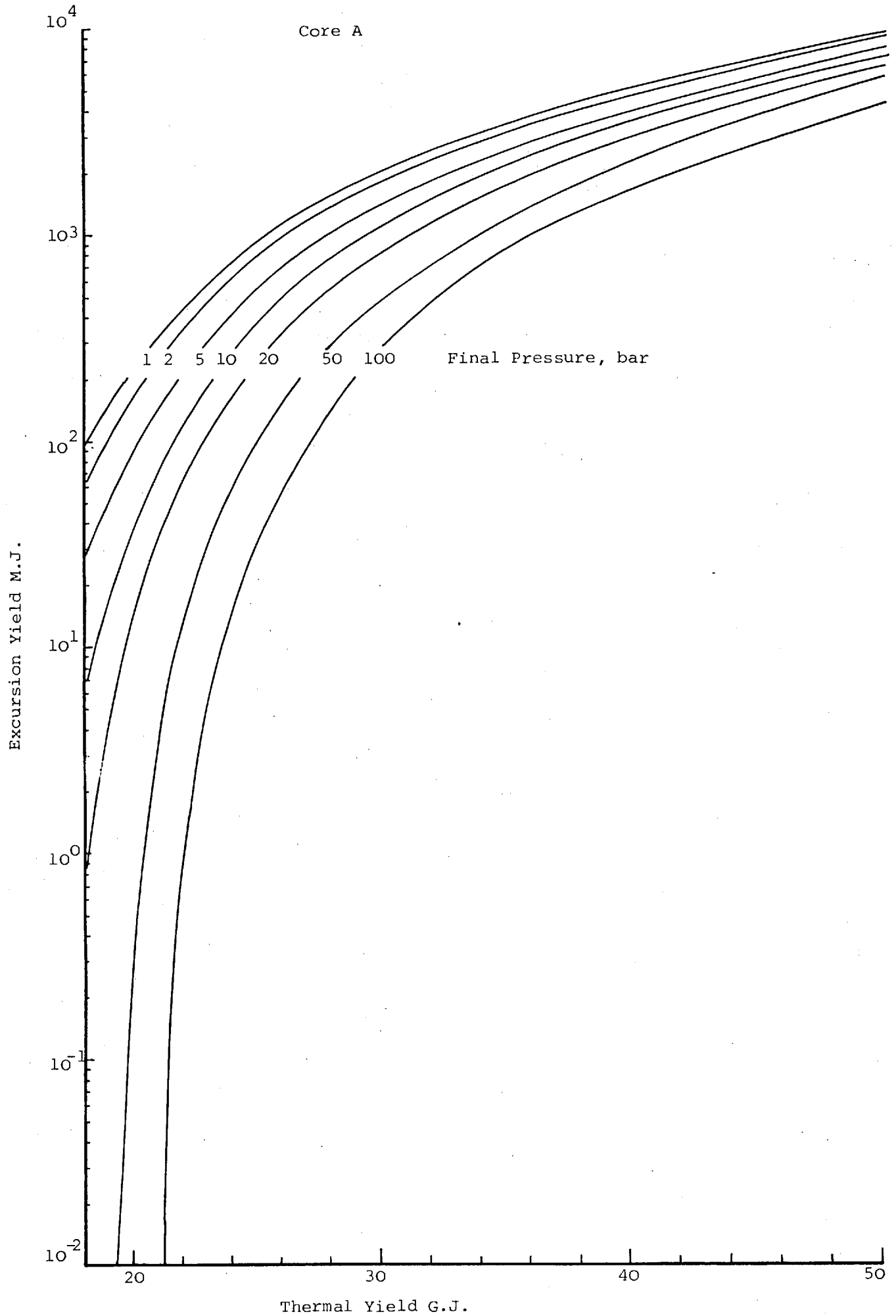


Figure 6.40b Effect of Final Pressure on Excursion Yield -

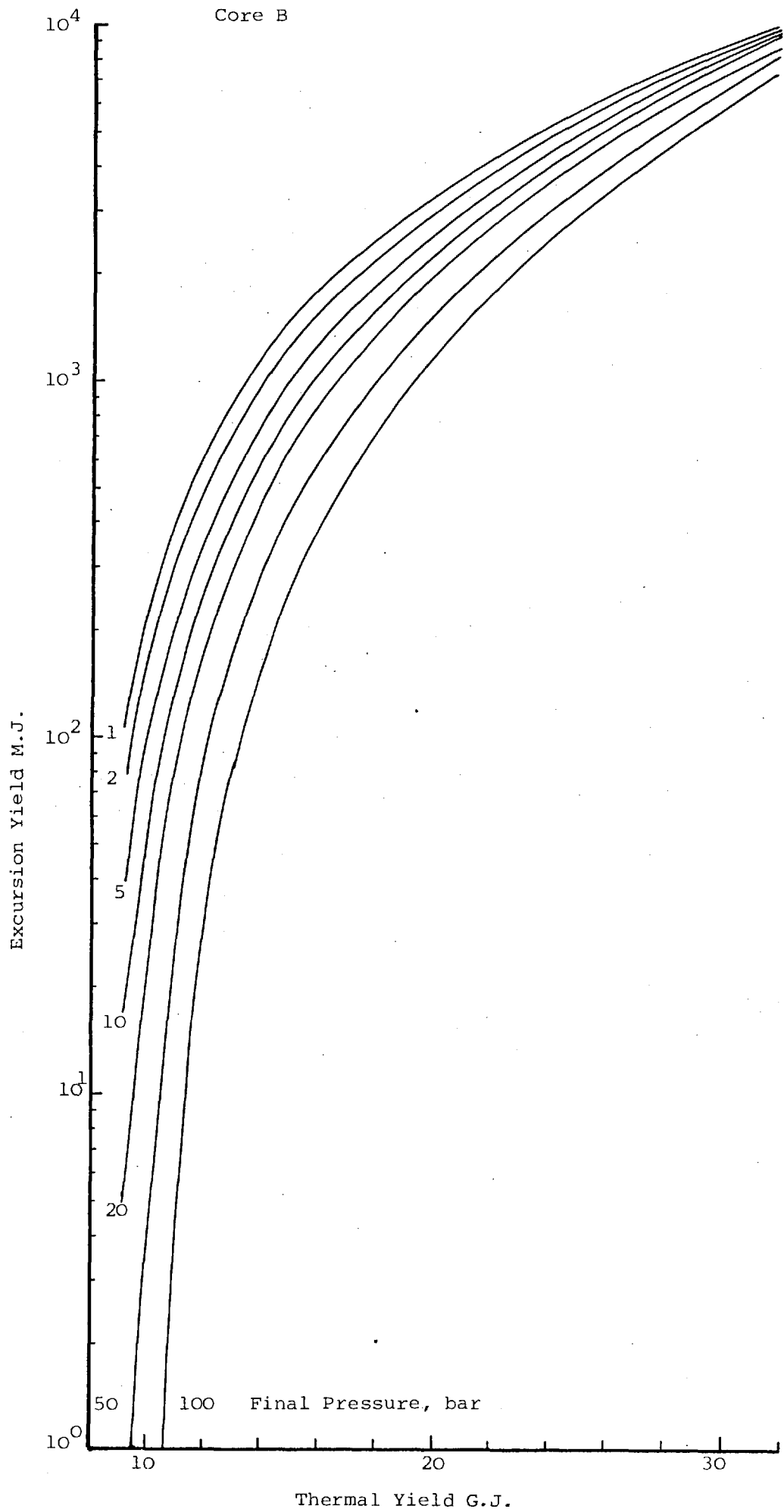


Figure 6.41a Effect of Final Pressure on Vapour Fraction -

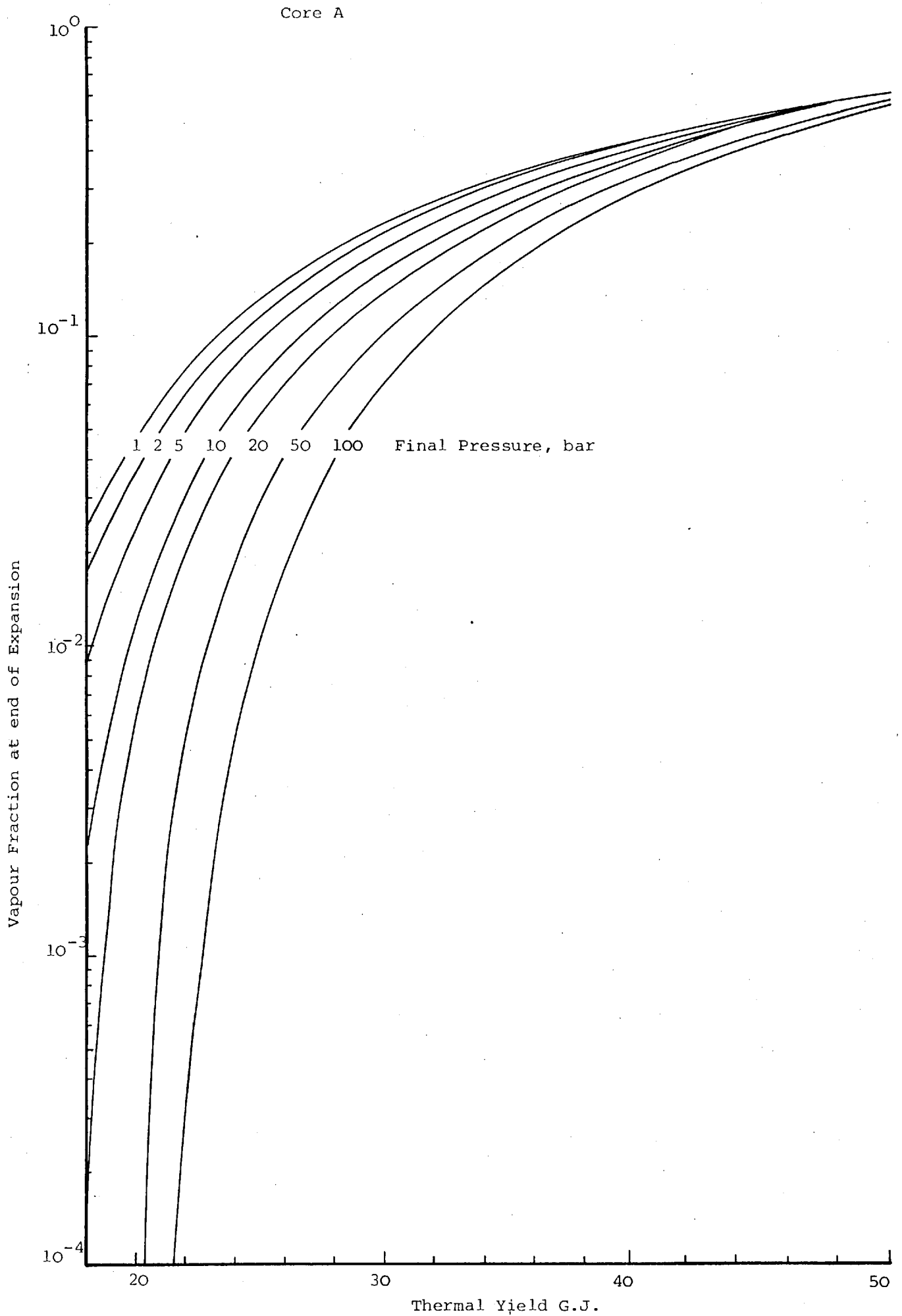


Figure 6.41b Effect of Final Pressure on Vapour Fraction -

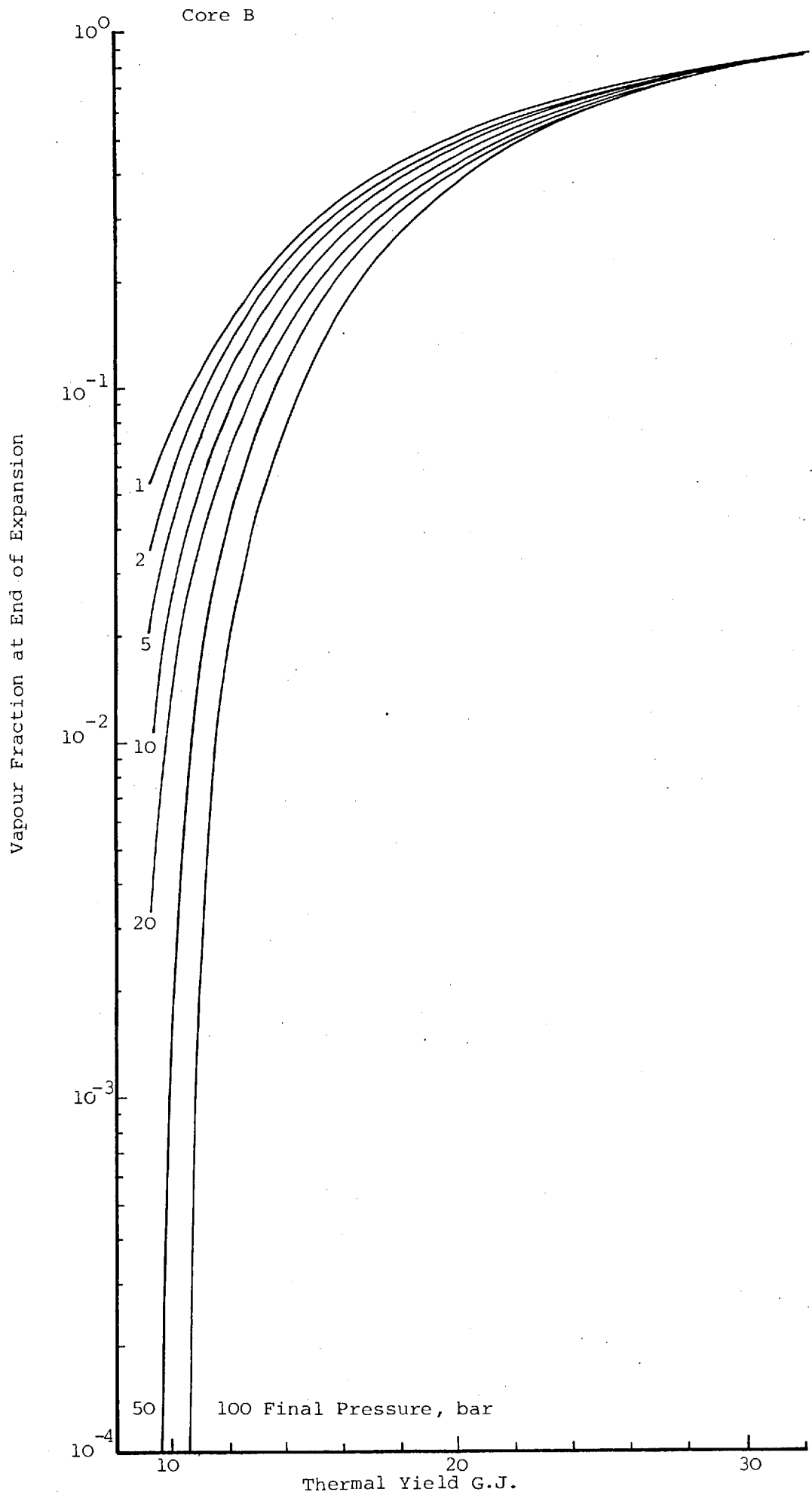


Figure 6.42a Effect of Final Pressure on Final Volume -

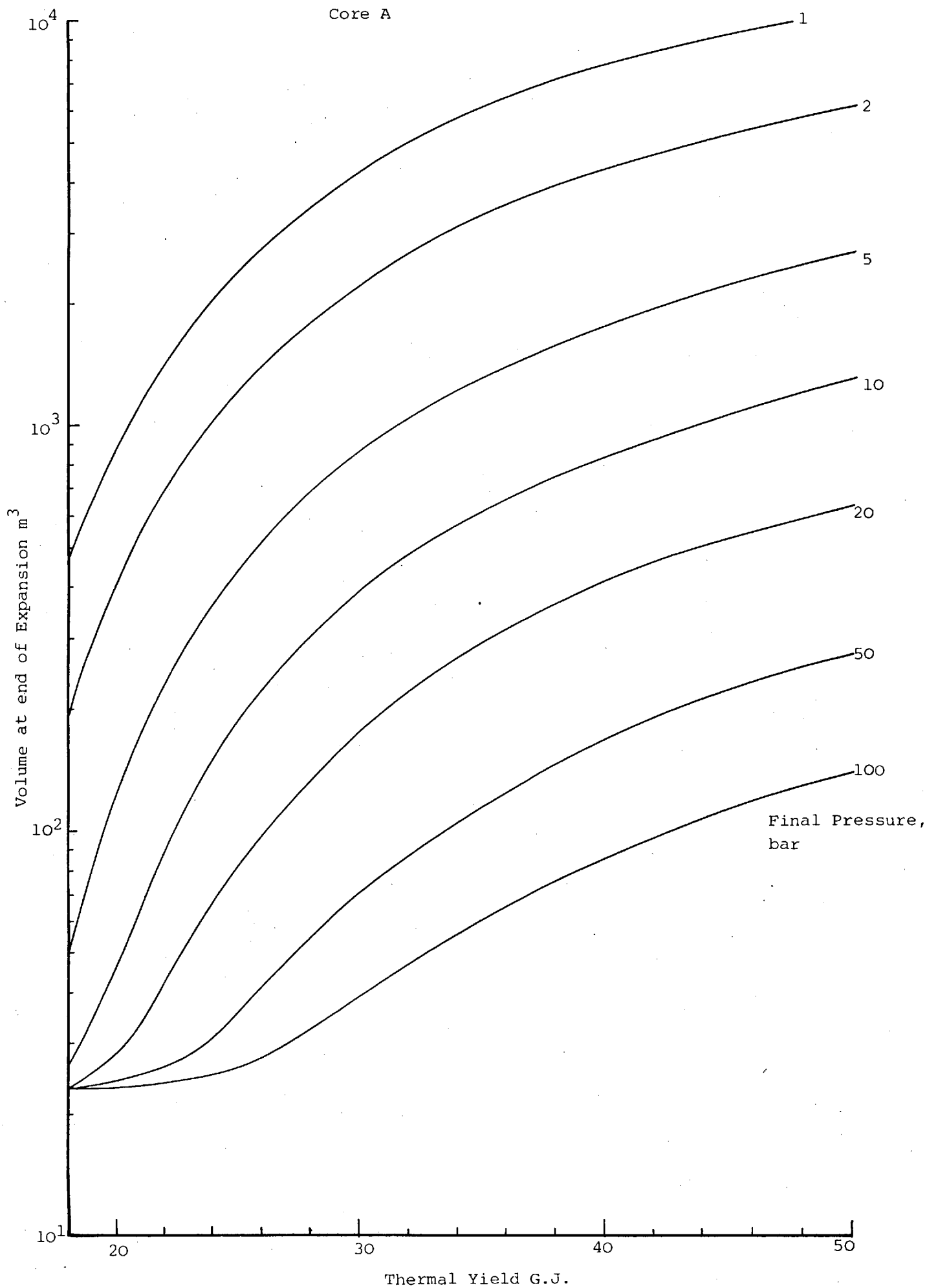


Figure 6.42b Effect of Final Pressure on Final Volume -

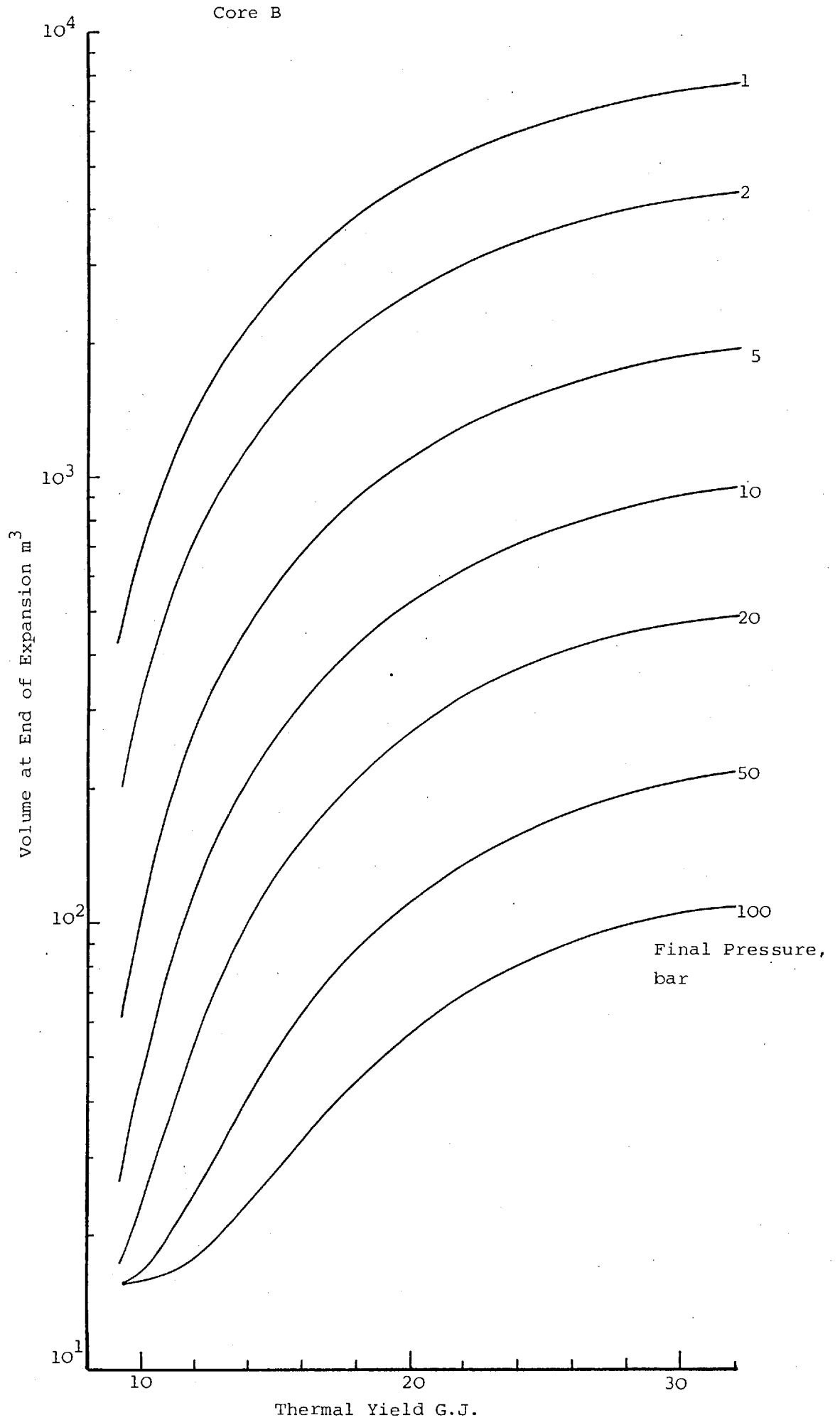


Figure 6.43a Variation of Excursion Yield along lines of

Constant Volume and Constant Thermal Yield - Core A

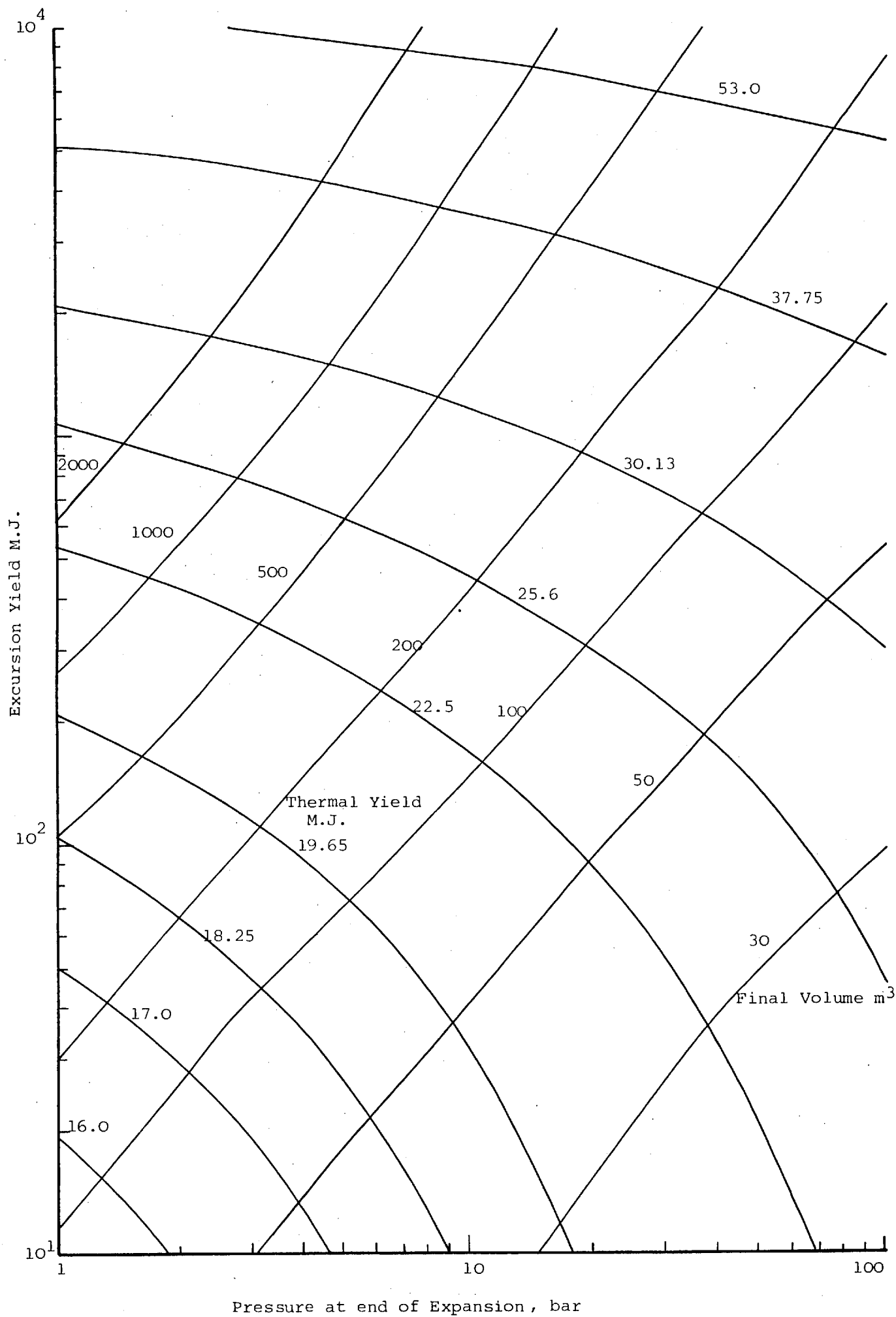


Figure 6.43b Variation of Excursion Yield with Final Pressure along lines of Constant Final Volume and Constant Thermal Yield - Core B

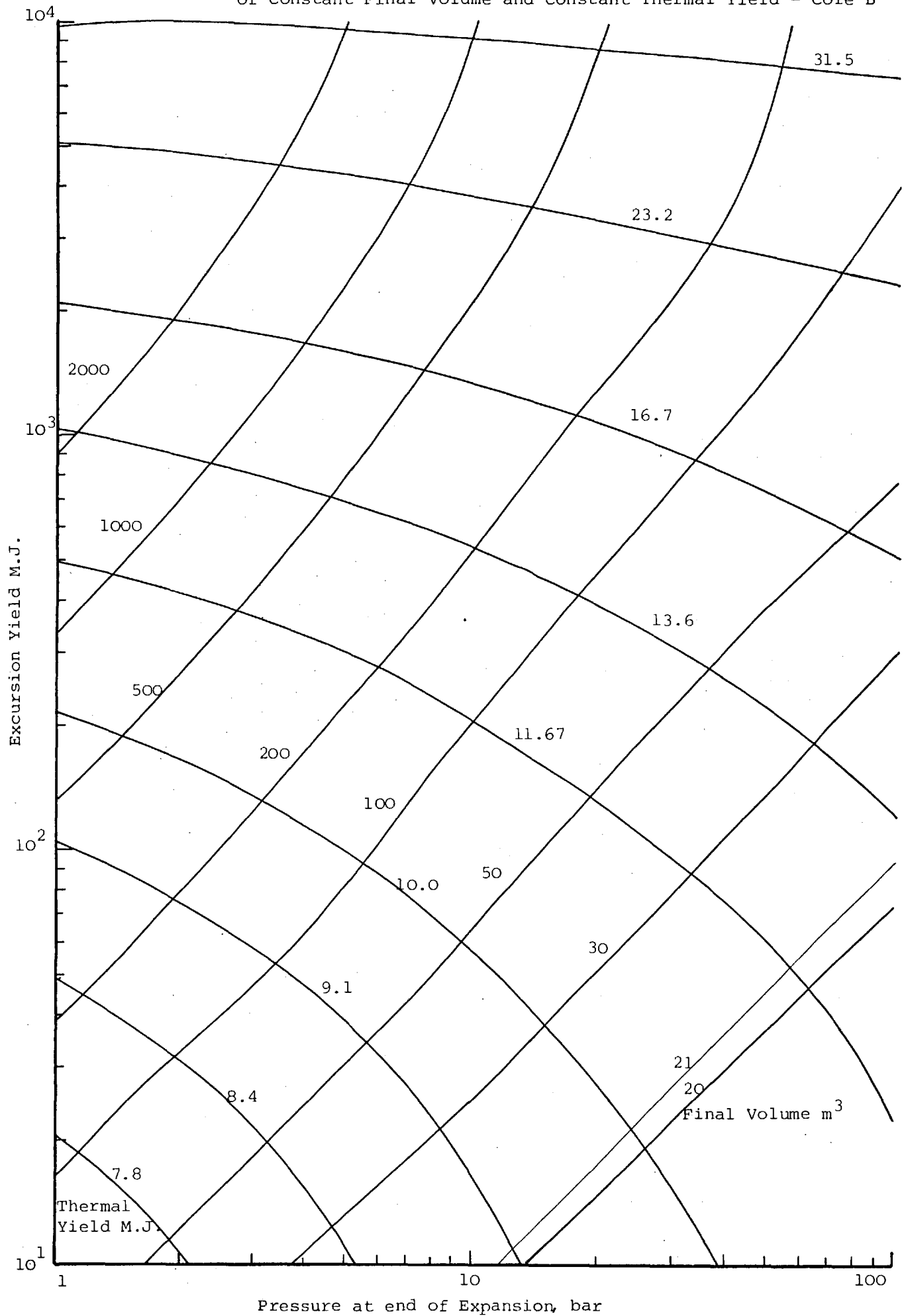


Figure 6.44a Variation of Vapour Fraction with Final Pressure along

lines of Constant Final Volume and Constant Thermal Yield -

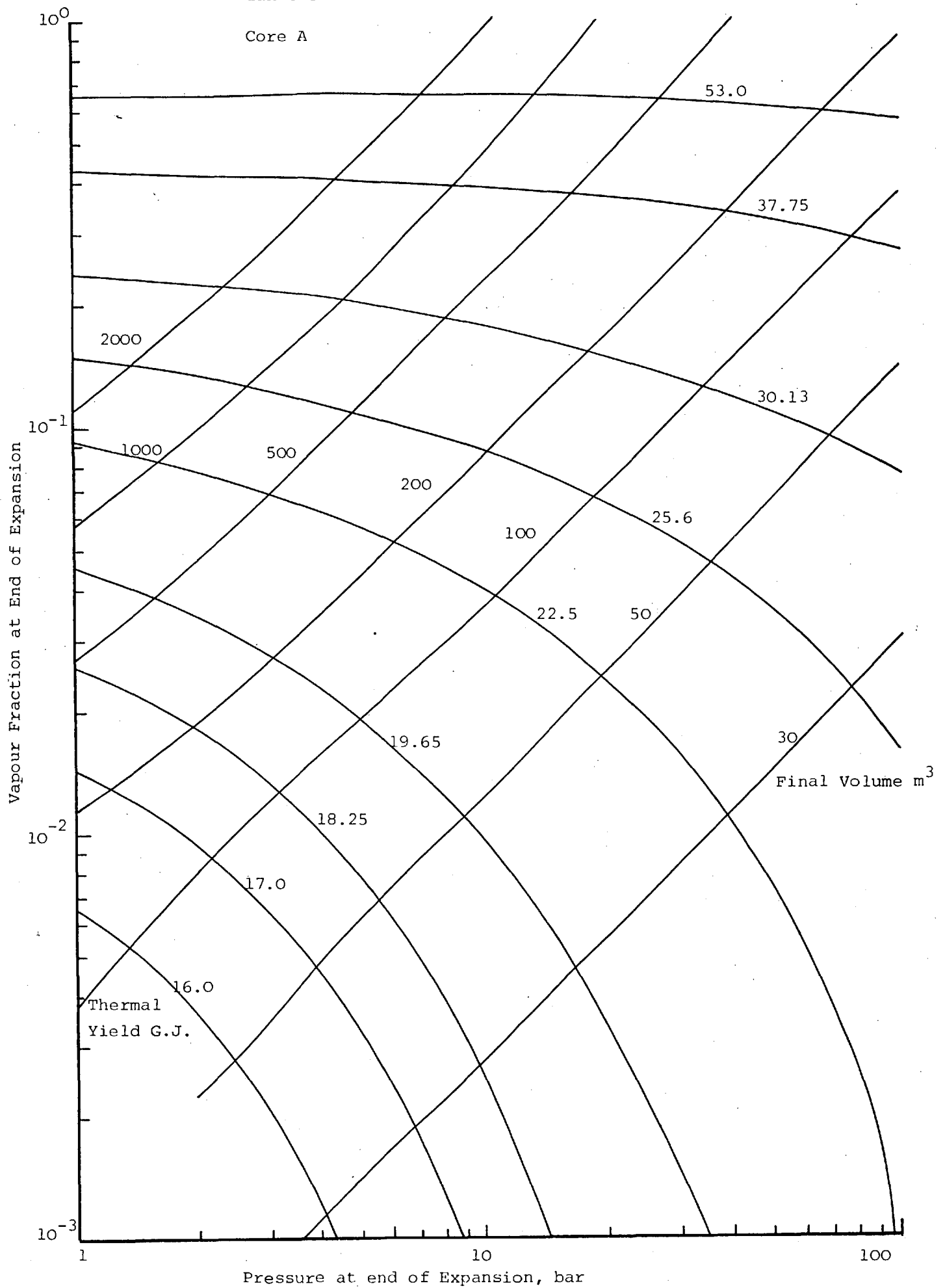
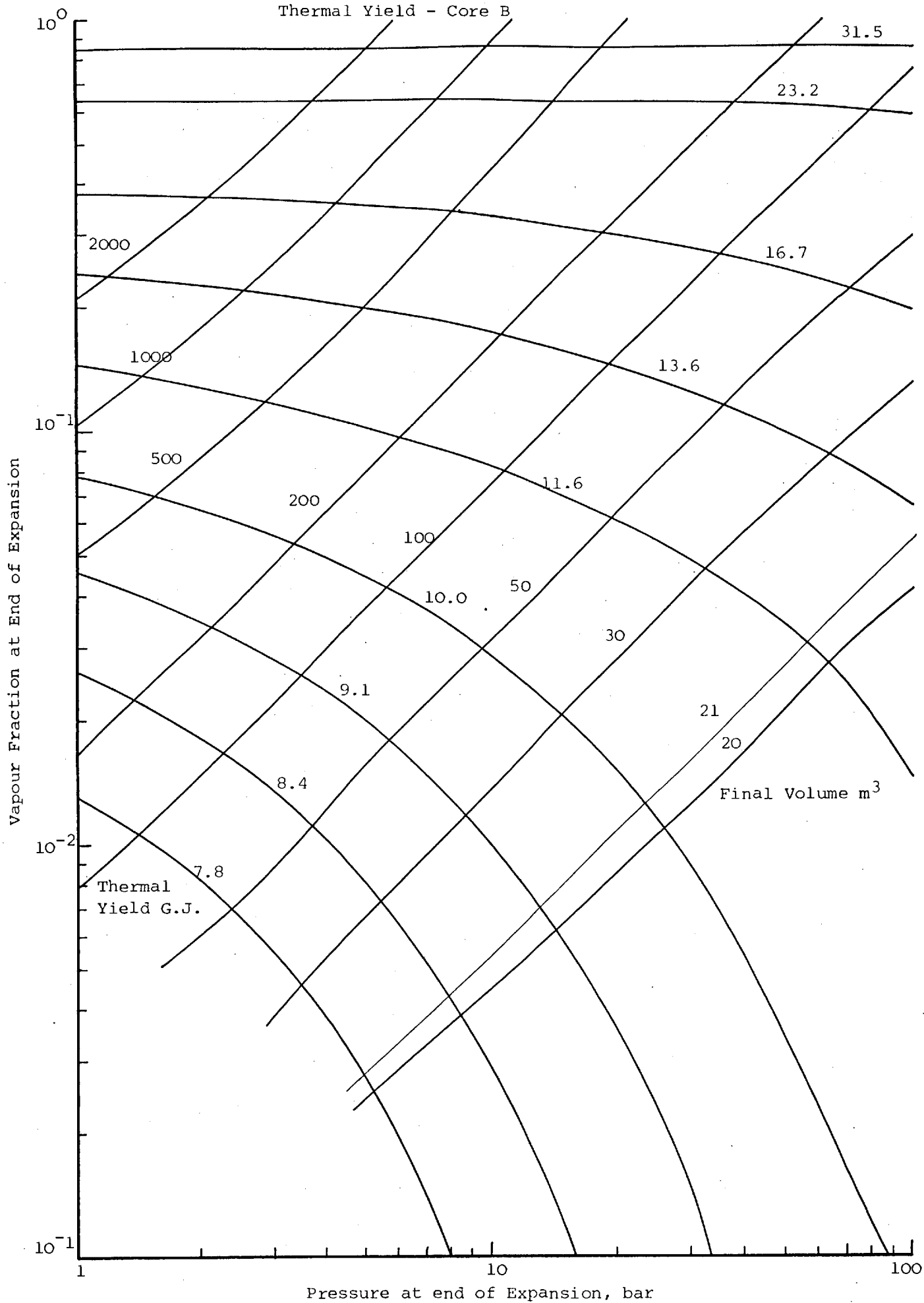


Figure 6.44b Variation of Vapour Fraction with Final Pressure along

lines of Constant Final Volume and Constant Final

Thermal Yield - Core B



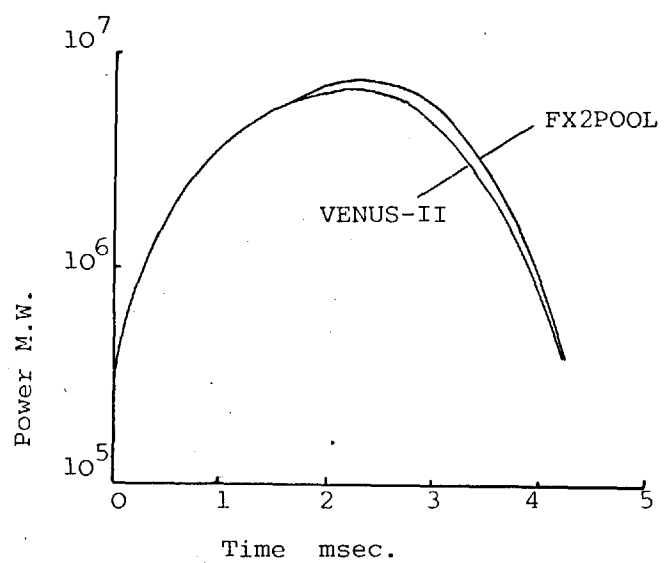


Figure 7.1 Power Comparison of VENUS-II and FX2POOL
(after 45)

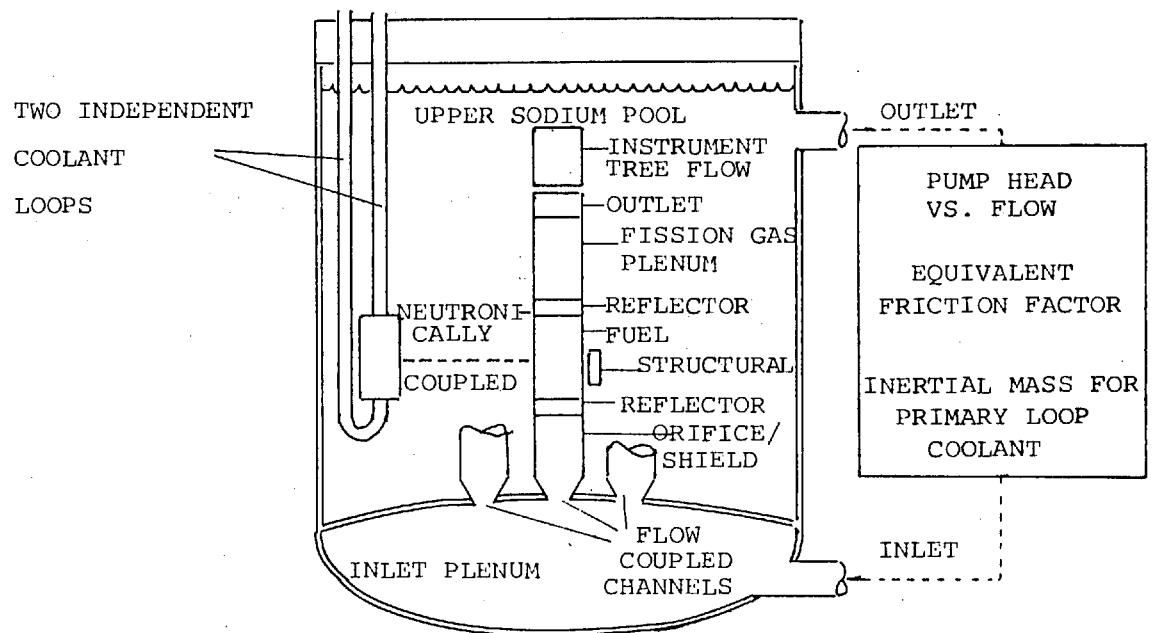


Figure A.3 MELT-III Model

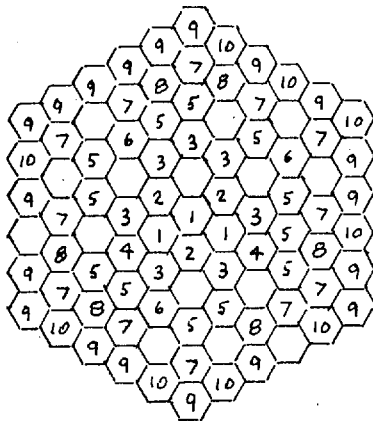


Figure A.1

Typical MELT-III Formulation - Ten Channel Simulation

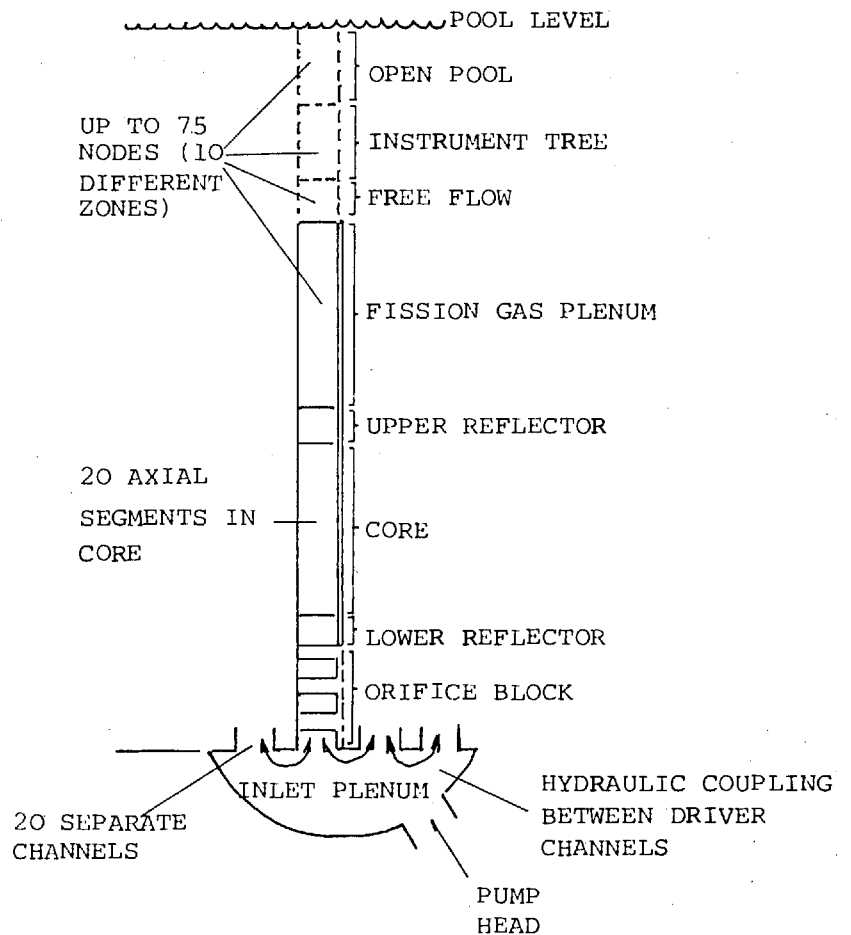


Figure A.2 MELT-III Hydraulic Geometry within the Vessel

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STUDIES OF FAST REACTOR DISASSEMBLY USING

A BETHE-TAIT COMPUTER CODE

by

J.C.LUDWIG

CORRIGENDA

<u>Page</u>	<u>Line</u>	<u>Is</u>	<u>Should be</u>
iv	16	Probability	Probablistic
v	22	Modes	Models
x	17	Codes	Cores
xv	15	Yield	Feedback
5	14	Loop and pool	Pool and loop
9	3	Remain	Remains
41	1		[]
46	11	>	>
55	22	Axial	Radial
62	23	The paragraph starting: The straightness of the should be replaced by the following:	

Equation 6.4 is not generally soluble so easily, as $f()$ is really itself a function of $Q(t)$ and t . However, the straightness of portions of the yield plots implies that under certain very limited conditions it is possible to obtain a 'simple' solution, such as eqn. 6.5. Here, Q_{final} represents the energy deposited at time $t=t_f$, where t_f is the actual duration of the transient obtained from the full solution of the full equations. Taking this argument one step further, one can say that in the regions where the yield plots are linear, the governing parameter in $f()$ is the doppler coefficient d thus enabling eqn. 6.5 to be rewritten as:

$$Q_{\text{final}} = D_{\text{exp}} (E d) \quad (E=\text{constant}) \quad 6.6$$

This equation is of course only approximately true at the best of times and has validity under a very narrow range of conditions, i.e. at very high ramp rates and doppler constants in the range -0.003, -0.007.

<u>Page</u>	<u>Line</u>	<u>Is</u>	<u>Should Be</u>
72	19	Fig.6.20a	Fig.6.20c
81	25	Variation	Variations
92	2,3	Effects of various implications	Implications of various effects
180		Fig.8c	Fig.6.8c
185	Label of y axis	$\bar{E}_B$	EA/EB
199	Limit of y axis	100	10.0
200		Fig.6.18	Fig.6.18a
231		Specific Heat	Latent Heat
232		Specific Heat	Latent Heat
240		(To=1400K)	(To=1500K)