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RESEARCH ESTABLISHMENT

LUCAS HEIGHTS

THE ENGINEERING DESIGN AND ANALYSIS ASPECTS OF THE BERYLLIA MODERATED PEBBLE BED REACTOR REFERENCE STUDY

by

D.R. EBELING
J.E. HAYES

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ABSTRACT

Design and analysis of a carbon dioxide gas cooled beryllia moderated pebble bed reactor system has been undertaken to assess its technical feasibility and economic potential from the engineering point of view. This report describes the design and shows how subsequent analysis indicated the likely costs of such a system and possible technical improvements.

It was found that data vital to a detailed design would not be available for some years and it was decided to proceed initially with a reference design based on optimistic predictions of these data. This was expected to indicate the materials and physics requirements necessary to achieve an attractive working design.

The two most important predictions from the initial work on neutron physics and materials properties were that a maximum value of 1.75 for the burnup (in terms of fissions per initial fissile atom (F.I.F.A.)) could be obtained with plutonium fuel from natural uranium reactors in which the fuel had been burned to 3000 MWd/tonne, and that fuel elements of 1–3/8 inch diameter should survive sufficiently intact in a core in which the coolant flow was upwards at an average core power density of $11 W/cm^3$ without an embarrassing fission product release due to an excessive number of failed fuel elements.

For this reference design a moderator–fuel ratio of 1650 was chosen. It was assumed that the fuel cycle for this design was open, that is, spent fuel was discarded. A 200 MWe unit upflow design is presented and the economics of such a system described and extrapolated to cover a range of sizes.

Further work using the reference design as a basis was done to investigate a closed fuel cycle and the effect of varying the major design parameters. Later nuclear analysis indicated that the burnup would be limited to a value for F.I.F.A. of about 1.4 for open cycle working. Economic analysis indicated that the best method of operating such a design in a 5000 MWe system would be as a one–pass core at a burnup (F.I.F.A.) of 1.0 – 1.2 with U233 recycle. Since the economics of the cycle tended to remain fairly constant over a wide range of moderator ratio a larger moderator ratio (2000–2500) could be chosen for the recycle system.

Calculations indicated that a desirable negative temperature coefficient and a satisfactory control and shutdown system should be achievable. However, to verify this a deep reactor physics investigation would be necessary, including hot critical assembly tests. The analysis of the fuel element thermal stress behaviour was also not conclusive because the material behaviour depends largely on fabrication techniques which are not fully established. There seems a good possibility that the requirements will be met but difficulties with reprocessing are expected and further investigations into thermal stress and fission product retentivity by means of a test bed or in–pile experiment would be essential to establish technical feasibility.

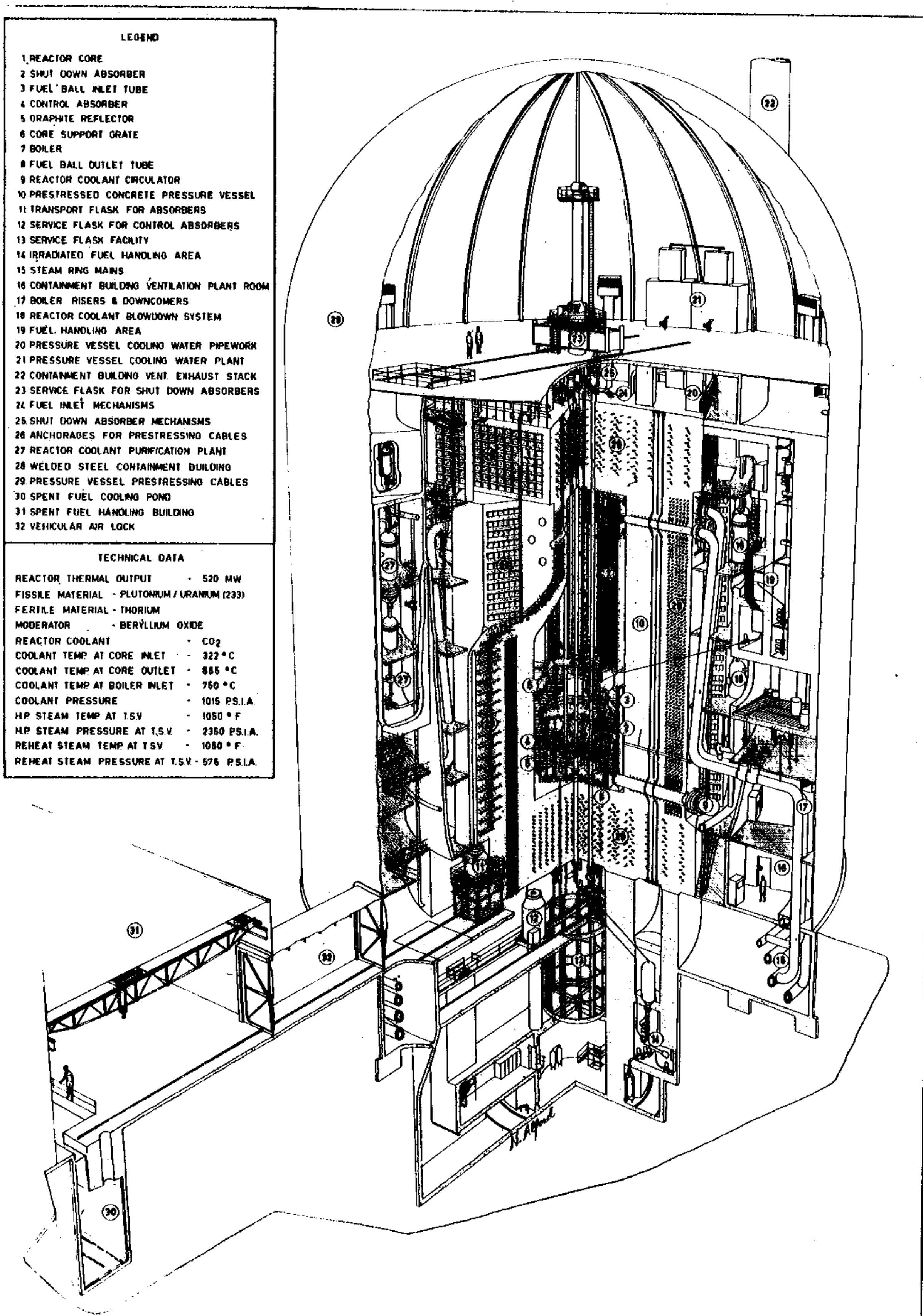
Comparison with the 2 x 500 MWe CANDU system shows that even with the presently most optimistic assumptions the reference design upflow P.B.R. system is economically inferior. It is suggested that further investigation of superposed irradiation damage, contact stresses, wear, and adhesion could lead to a feasible downflow design. When combined with investigations of approach to equilibrium, chemical reprocessing, and topping cycles this might lead to significant improvements in unit costs but at this stage it seems unlikely that a system could be devised which would be sufficiently attractive to justify its development as a large power station.

LEGEND

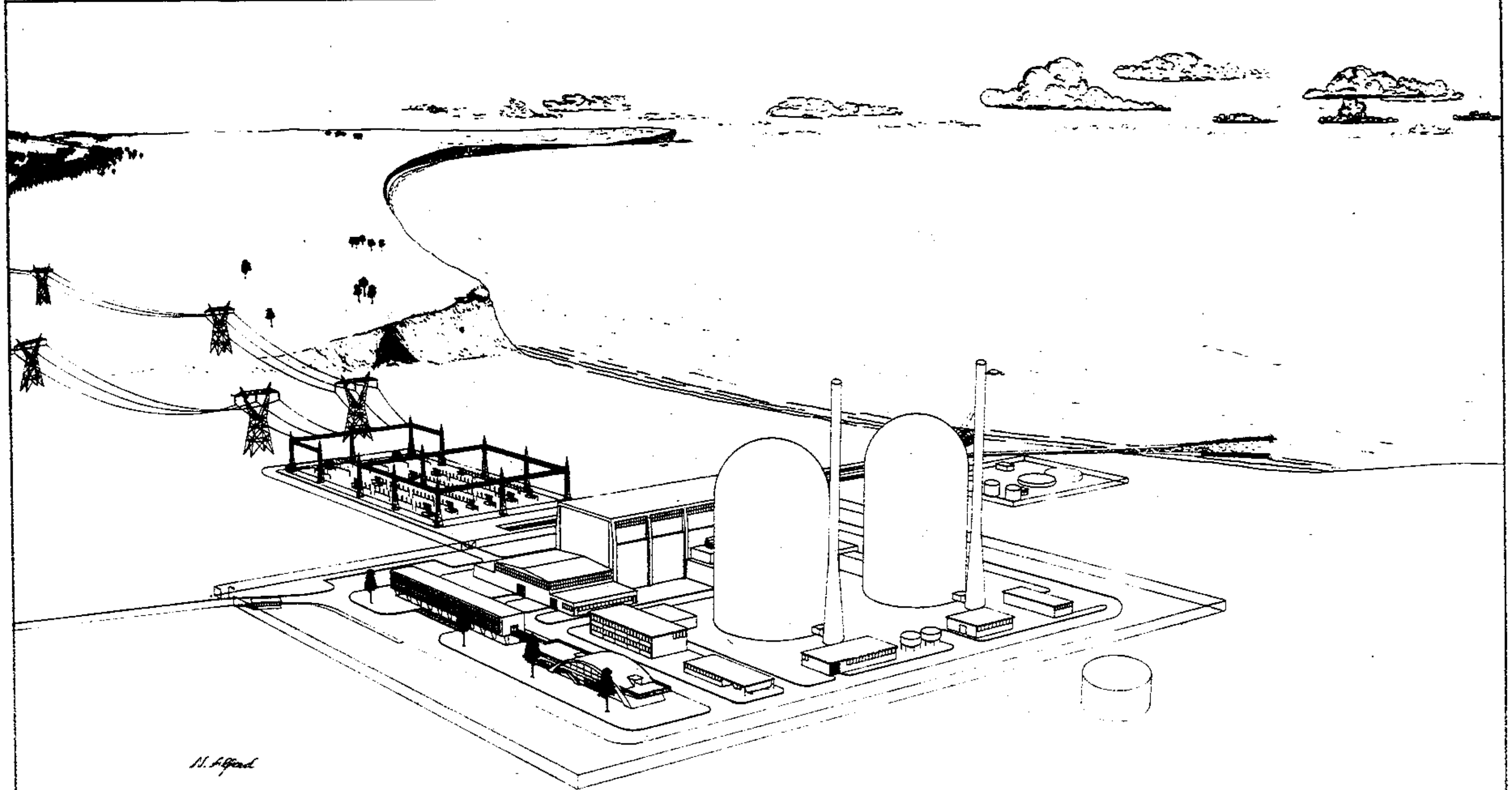
1 REACTOR CORE
2 SHUT DOWN ABSORBER
3 FUEL BALL INLET TUBE
4 CONTROL ABSORBER
5 GRAPHITE REFLECTOR
6 CORE SUPPORT GRATE
7 BOILER
8 FUEL BALL OUTLET TUBE
9 REACTOR COOLANT CIRCULATOR
10 PRESTRESSED CONCRETE PRESSURE VESSEL
11 TRANSPORT FLASK FOR ABSORBERS
12 SERVICE FLASK FOR CONTROL ABSORBERS
13 SERVICE FLASK FACILITY
14 IRRADIATED FUEL HANDLING AREA
15 STEAM RING MAINS
16 CONTAINMENT BUILDING VENTILATION PLANT ROOM
17 BOILER RISERS & DOWNCOMERS
18 REACTOR COOLANT BLOWDOWN SYSTEM
19 FUEL HANDLING AREA
20 PRESSURE VESSEL COOLING WATER PIPEWORK
21 PRESSURE VESSEL COOLING WATER PLANT
22 CONTAINMENT BUILDING VENT. EXHAUST STACK
23 SERVICE FLASK FOR SHUT DOWN ABSORBERS
24 FUEL INLET MECHANISMS
25 SHUT DOWN ABSORBER MECHANISMS
26 ANCHORAGES FOR PRESTRESSING CABLES
27 REACTOR COOLANT PURIFICATION PLANT
28 WELDED STEEL CONTAINMENT BUILDING
29 PRESSURE VESSEL PRESTRESSING CABLES
30 SPENT FUEL COOLING POND
31 SPENT FUEL HANDLING BUILDING
32 VEHICULAR AIR LOCK

TECHNICAL DATA

REACTOR THERMAL OUTPUT	520 MW
FISSILE MATERIAL	PLUTONIUM / URANIUM (233)
FERTILE MATERIAL	THORIUM
MODERATOR	BERYLLIUM OXIDE
REACTOR COOLANT	CO_2
COOLANT TEMP. AT CORE INLET	322 °C
COOLANT TEMP. AT CORE OUTLET	855 °C
COOLANT TEMP. AT BOILER INLET	750 °C
COOLANT PRESSURE	1015 P.S.I.A.
H.P. STEAM TEMP. AT T.S.V.	1050 °F
H.P. STEAM PRESSURE AT T.S.V.	2350 P.S.I.A.
REHEAT STEAM TEMP. AT T.S.V.	1050 °F
REHEAT STEAM PRESSURE AT T.S.V.	576 P.S.I.A.



ARTIST'S IMPRESSION OF 206 MWe UPFLOW PEBBLE BED REACTOR



ARTIST'S IMPRESSION OF A TYPICAL TWIN 200 MWe H.T.G.C.R. REACTOR POWER STATION

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1. INTRODUCTION

A study of the feasibility of a gas cooled beryllium oxide pebble bed high temperature reactor was commenced in September 1963 by the Research Establishment of the A.A.E.C. (Alder and Roberts 1964). The nuclear engineering design aspects of this work were carried out in the Engineering Research Division. Since this part of the work deals with the reactor as a whole, the following report could be regarded as the focal point of the study; however, other reports will deal with aspects of materials development, nuclear data preparation, and experimental engineering research.

In 1962 the U.S.A.E.C.'s Report to the President forecast that the likelihood of a rise in the price of uranium ore towards the end of this century could result in less economic operation of natural and low enrichment systems. This, coupled with depletion of world resources of natural fissile material would necessitate adoption of an alternative reactor system. Assuming successful development of a fast breeder reactor with less than a 12 – 15 year fuel inventory doubling time, the problem of future low cost power generation should be solved in large power systems using unit sizes above 500 MWe. In smaller power systems, requiring medium sized generating units in the range 200 MWe to 500 MWe, the fast breeder reactor might be significantly less economic due to decrease in neutron economy with decrease in size. The main justification for studying the H.T.G.C.R. concept was, therefore, that in combination with the natural or low enrichment system, it might produce the lowest generating cost during the period prior to the development of a power system of adequate size to utilize the fast breeder cycle economically.

Early in the study a reference design of 200 MWe unit size was chosen (Gerrand et al. 1966) to establish design parameters for more intensive analytical and experimental work, but consideration of the concept over a unit size range from 100 MWe to 500 MWe was to be an important aspect of the study. It was expected to be a competitor with the similar, helium–graphite H.T.G.C.R. concepts but would have the advantages of a cheaper coolant (CO_2), fewer safety problems, and superior nuclear performance, resulting in lower fuel and plant costs and possibly improved utilization of fertile material.

This report describes how the reference design study was conducted by assuming the major parameters in order to establish the detailed design, and how this was used in subsequent analysis to indicate the possible improvements and establish the likely costs. Some indication is given of the direction which future research should take.

2. BASIS OF DESIGN

In the Commission's study of the technical and economic feasibility of a high temperature gas cooled reactor (H.T.G.C.R.) in which the core is generally composed of ceramic materials the following specifications were given to the evaluation team:

(i) The Fuel Element Material: a dispersion of oxide fuel particles in sintered beryllia in a form which best satisfies heat transfer and fission product retention requirements.

(ii) The Fuel Element Form: spherical, since it was thought that prismatic elements would distort and be difficult to handle. Also, at the high level of burnup required for economic operation, annealing of the BeO a number of times during its lifetime might be necessary to alleviate irradiation damage. The pebble bed scheme avoids the first problem and makes the second easy to solve. It was accepted that poorer economics might result from high core pressure drops and the combination of the expensive BeO moderator with the fuel.

(iii) The Moderator: should be basically BeO.

(iv) The Coolant: carbon dioxide gas which is cheap and in plentiful supply in Australia and elsewhere.

(v) The Fuel: plutonium, produced by natural uranium fuelled reactors. Alternative use of U235, U233 and recycle of U233 from thorium conversion were also to be investigated. Thorium was chosen as the fertile material because of the neutron production capabilities of the bred U233 and the relative abundance of thorium in most parts of the world.

(iv) The Reactor Size: variable in the range 100 MWe to 500 MWe with the main reference design at 200 MWe which is a unit size likely to be appropriate for medium-sized power systems.

3. DESCRIPTION OF THE UPFLOW REFERENCE DESIGN

The design study was mainly concerned with determining the major technical problems in design of the reactor core and primary coolant circuit. By assuming the major parameters and establishing a detailed reference design it was possible to determine most of the areas of uncertainty and establish approximately the optimum design conditions.

Most of the detailed design effort was devoted to aspects of the reactor as shown in Figure 1 but consideration was given to the design of the ancillary plant as shown in Figures 2a, 2b, and 2c and the power station design and site layout as shown in Figure 3 and the Frontispiece.

3.1 Design Decisions

During the early stages of the study it was found that data vital to the detailed design would not be available for some time and a number of design decisions were thus required which at that time were difficult to justify completely. It was decided to proceed, using optimistic assumptions, as follows:

(i) Direction of Coolant Flow Through the Core: A number of coolant flow directions were considered (Figure 4) but all but the axial upward and downward directions were quickly eliminated as they required some form of structure within the core to control the coolant flow direction. As replacement during reactor life seemed inevitable to prevent excessive irradiation damage, permanent in-core structures were considered impracticable.

The restriction that levitation of the core placed on upflow designs led to the initial choice of the downflow coolant system. It was, however, soon apparent that serious technical uncertainties were associated with the design of the support grate structure, the effect of high contact forces on wear and adhesion of fuel elements, and coolant flow instability at low circulation rates. The upflow coolant system was therefore selected for the reference design, in spite of the expected economic disadvantage due to the need for somewhat higher coolant pressure and lower power densities to prevent levitation. The final proof of technical feasibility of the upflow system, unlike the downflow system, depends only on fuel element integrity.

(ii) Fission Product Retention: The fuel was assumed to be sufficiently retentive to give primary circuit activities which would permit release of the primary coolant (CO_2) direct to atmosphere in the event of a primary pressure circuit failure. This condition must be met if full economic advantage is to be taken of the concept of the fully ceramic beryllia dispersion fuel.

(iii) The Thermodynamic Cycle: The steam cycle was chosen as the 1050 °F (565 °C), 2400 p.s.i. reheat steam cycle in use in the most modern fossil fuelled power plants. As the reactor coolant temperature (865 °C) is more than adequate for the steam cycle, the possibility exists for incorporating a "topping" device if this is proved an economic advantage.

(iv) System Size: This was arbitrarily set at 5000 MWe in order to determine fuel recycle plant through-put to establish the effect on fuel cycle cost.

(v) The Fuel Cycle: This was initially chosen as an open cycle because of expected high burnup. A value for F.I.F.A. (fissions per initial fissile atom) of 1.75 was later thought to be the absolute maximum which could be expected for a 200 MWe reactor and this was adopted for the reference design. A moderator–fuel atom ratio of 1650 was selected as well as a fertile to fuel atom ratio of 16.5.

3.2 Parameter and Data List

The main parameters and data listed in Appendix 1 have been abstracted from a more complete listing(1)* which contains comment on the validity of the assumptions and gives sources of information.

It should be noted that the original calculations were carried out using the FLOAT code(26) but a more sophisticated code (TASPOP), which includes the effect of cross–flow and voidage variation, has produced more precise figures (Binns 1966a). In view of lack of experimental confirmation of the assumptions and the fact that the TASPOP results at that stage were only preliminary, the FLOAT results are used herein.

The most significant parameters are listed below for quick reference:

	Reactor Thermal Output (total)	520.0 MWh
	Station Net Electrical Output	206.0 MWe
	Station Overall Efficiency	39.6%
	Core Diameter (Equiv. cylinder)	15.21 ft.
	Core Average Height	9.13 ft.
	Average Voidage	40%
	Fuel Element Diameter	1.37 ± 0.030 in.
**	Average Burnup (F.I.F.A.) (Open Cycle)	1.75
**	Fuel Composition (PuO_2:ThO_2:BeO moderator ratio)	1:16.5:1650
	Average Power Density	11.0 W/cm^3
	Coolant	CO_2
	Coolant Working Pressure	1000 p.s.i.g.
	Bulk Coolant Temperature at Core Outlet (before mixing)	865 °C
	Coolant Temperature at Circulator Inlet	321 °C
	Peak Surface Temperature of Fuel Element	1259 °C
	Peak Centre Temperature of Fuel	1292 °C
	Average Temperature of Fuel	680 °C
	Average Fuel Lifetime in Core	5.2 years
	Average Fissile Heat Rating (based on initial fuel)	0.83 MW/kg

* Numbers in parentheses refer to unpublished works.

** Since the F.I.F.A. might vary between about 1.0 and 1.8 depending on the fuel, and the composition can vary between 1:20:1000 to 1:6:3000 depending on the cycle (see Section 4.2), the above values were initially selected to enable a complete design to be presented for reference and optimization.

Number of Boilers (once–through Benson type)	6
Superheat Pressure	2400 p.s.i.a.
Superheat Temperature	1055 °F (568 °C)
Reheat Outlet Pressure	583 p.s.i.a.
Reheater Outlet Temperature	1051 °F (565 °C)
Average No. of Fuel Element Transits through Core	6
Average Fuel Ball Rate per Extract Point	24/hr
No. Extract Points	7
Absorber Reactivity Required for Shutdown	6.3% $\Delta k/k$
Net Reactivity Worth of Shutdown Rods (allowing 2.5% $\Delta k/k$ for failure)	7.5% $\Delta k/k$
Net Reactivity Worth of Control Absorbers	1% $\Delta k/k$

3.3 Reactor Core, Reflectors, Internals, and Shielding

3.3.1 Reactor core

The pebble bed reactor core consists of an assembly of over one million 1⅜ inch (3.5 cm) diameter, spherical fuel elements contained within a vessel which forms the boundary between the core and the fixed reflectors. The arrangement of the core is shown in Figure 5a.

An early comparison of upflow and downflow concepts indicated that the performance of the upflow concept would be limited by the need to prevent even partial levitation of the core by the upward coolant flow. As the study proceeded, several design features, for example tapered core sides and bypass of coolant, were found to alleviate the effect of the levitation limit on core performance. A description of these features and their effect on core performance is given in Section 5.1.3.

Details of the core container vessel design(2) are shown in Figures 5a and 5b. A main feature is "impingement" cooling in which use is made of the bypass flow of coolant to maintain the temperature of the vessel walls well below that of the core. This is achieved by passing coolant through a network of small holes in the outer zirconium alloy vessel, onto a thin, 1/16 inch thick, stainless steel sheath attached to its inside surface. The sheathing is corrugated to permit differential thermal expansion between points of attachment to the outer vessel. With a coolant inlet temperature of 322 °C, it is expected that the temperature of the stainless steel sheath would not exceed 650 °C and that of the zirconium alloy vessel 400 °C.

It is intended that the core should remain at or close to working temperature during all phases of normal operation and shutdown. It is conceded that after some accident or during initial start–up the bed would commence cold and then rise to working temperature, so thermal expansion in the radial direction must be considered. The German A.V.R. experimental team showed that the graphite bed accommodated itself to this condition without significant increase in side pressure. Since the BeO balls have a greater coefficient of friction it cannot be assumed that the same condition will occur and experiments are under way to check this effect. It is considered however that the tapered bed will assist in lowering side pressures and it will certainly be minimized by continuous operation of the extract devices if the rate of rise of temperature is reasonably slow.

3.3.2 Fuel element

Alternative fuel element designs are shown schematically in Figure 6. The reference design fuel element is of simple spherical shape, of 1⅜ inches (nominal) diameter (see Figure 6a).

The element consists of a dispersion of plutonia/thoria particles (150–200 μ) in fine grain, (5 μ) beryllia contained within and bonded to a fuel–free beryllia shell of 0.050 inch (nominal) thickness. The element is sintered to obtain a density of at least 97 per cent. theoretical, and the properties of the material were assumed to be those expected at the commencement of the project (Smith 1964).

The outer, fuel–free shell serves to prevent:

(1) Release of fission products by direct recoil from the element surface.

(2) Loss of fuel–bearing material from the element due to minor mechanical damage, wear, and corrosion.

The performance of the reference design fuel element and that of a number of alternative designs, is described in Section 5.2. The design having the most potential for improved performance is that employing a thin graphite interlayer between the fuel dispersion and the fuel–free shell (Binns 1966b) See Figure 6d.

3.3.3 Reflector and core support structure

The design of the graphite reflector is shown in Figure 5b. The radial reflector consists of a self–supporting assembly of keyed blocks mounted on a steel structure which transfers the weight of the reflector to the lower end slab of the pressure vessel. The reflector is contained within the annular space between the zirconium alloy core vessel and a mild steel vessel as shown in Figure 5a.

The upper reflector consists of blocks individually suspended on zirconium alloy rods which also support the stainless steel sheathed zirconium alloy section forming the upper surface of the hot coolant plenum chamber. The rods are connected to a mild steel structure which also supports the six boiler units and transfers the combined loading, via mild steel columns, to the lower end slab of the pressure vessel. The zirconium alloy reflector lining behind the stainless steel sheet (Figure 5b) surrounding the hot plenum chamber has been designed to support the weight of upper reflector and that section of the radial reflector that is above the core, in the event of partial or complete failure of the normal means of support.

The lower reflector also forms the support grate for the core. It consists of slotted graphite blocks, assembled, as shown in Figure 5b, to form passages for entry of the main coolant stream to the core. The coolant passages are 5/8 inch wide to minimize interference with flow of the 1⅜ inch dia. fuel balls over the upper surface of the reflector. The coolant passages are offset to prevent direct streaming of radiation through the lower reflector. The upper surface of each block is protected against abrasion and chipping by a zirconium alloy "cap". The outer edges of the lower reflector extend into the radial reflector to ensure continuity of the core support grate at its junction with the side walls to prevent lodging of fuel in a clearance gap at this point.

The lower reflector and support structure is divided into sections about each of the seven fuel extract tubes. A cold clearance of 0.25 inch between adjacent sections provides for differential thermal expansion between the support structure (322 °C) and the reactor pressure vessel (60 °C). There is therefore no lateral movement of the fuel extract tubes during changes of coolant temperature and each of the support structure sections may be simply supported by a fixed pedestal.

3.3.4 Reactor internals

The general arrangement of the reactor (Figure 1) shows that the entire reactor coolant circuit is enclosed within the reactor pressure vessel. Expensive and possibly unreliable high pressure ducting is therefore eliminated.

The internal coolant ducting divides the coolant flow at entry to the core into the main flow through the core and a bypass flow through the radial and upper axial reflectors, as shown in Figure 5b. The proportion of bypass to main flow determines the coolant temperature at core outlet and is controlled by the 12 pneumatically operated bypass valves.

The shape of the hot plenum chamber and the ducting connecting it to the six boiler units is designed to reduce radioactivation of boiler components by neutron streaming to a sufficiently low level to permit access, during shutdown conditions with core removed, to the boiler units for maintenance. However, access would only be possible if the radiation from "plated-out" fission products is at a tolerable level.

The shape of the hot plenum chamber and connecting ductwork is also expected to effect efficient mixing of the main and bypass coolant flows to obtain uniform coolant temperature at entry to the boilers. The total pressure drop in all parts of the ducting, while high in the absolute sense at 1 p.s.i., is small compared with the overall pressure drop, 9.7 p.s.i.

The coolant returning from the boiler units to the circulators flows over the internal surface of the reactor pressure vessel and internal support structures, and also through the steel thermal shielding, to maintain a reasonably uniform temperature in these components.

Should it be necessary to isolate a boiler unit owing to a tube failure, special bypass valves, two to each unit, pass coolant from the cold plenum beneath the core to the base of the isolated boiler unit. By this means, overheating of the failed boiler and other primary circuit components is prevented and the reactor may continue to operate continuously at up to 80 per cent. rated output. Expensive and potentially unreliable boiler isolation valves are eliminated. Long operating experience with boilers under similar conditions in fossil fuelled stations combined with rigorous inspection should make complete failure of a boiler unit unlikely. However, should this occur, entry for repairs could be gained by penetrating the pressure vessel wall. This would be a difficult and expensive operation whose cost would have to be weighed against that of continuing to operate at reduced power for the remainder of the planned reactor life.

3.3.5 Shielding

The radiation shielding is designed to preserve the ductility of the pressure vessel liner, the boiler tubing and the internal structural components of the reactor. The pressure vessel walls in themselves form a more than adequate biological shield. Direct streaming of radiation through the boiler inlet ducts and the circulator penetrations has been mitigated but the effectiveness of the proposed designs, particularly in preventing long-term activation of components, has not been checked.

The results of the bulk shielding calculations, using the MAC code (3), are shown in Figure 7. The thermal shield, composed of boron steel, is effective in limiting fast neutron dose and gamma ray heat deposition in the concrete pressure vessel to acceptable levels. The fast neutron dose in other structural components is also reasonable with the exception of the stainless steel sheathing on the core container vessel. The recently discovered embrittling effect of (n, α) reactions in nickel (>3 MeV) means that, as in many other reactors, the in-core components may become embrittled and completely lose their ductility towards the end of their life. Since the design of the sheathing is such that there is very low load on this component, and as no shock loads are envisaged it is difficult to see how this component could fail. The control rod sheaths may be in a different category but these are replaceable.

3.4 Reactor Pressure Vessel

3.4.1 Choice of vessel type

A comparison of a wide range of pressure vessel designs (4) indicated that a cylindrical prestressed concrete pressure vessel containing both the reactor core and the boiler units is economically and practically superior to alternative welded steel designs.

A prestressed concrete pressure vessel is intrinsically safer than a welded steel pressure circuit. Catastrophic failure is unlikely because of the multiplicity of individual prestressing cables. Also, the large, easily monitored, strain in a prestressed concrete structure approaching failure facilitates early detection of a potentially dangerous condition. The same cannot be said of welded steel circuits in which an undetected microscopic flaw could cause a catastrophic failure without warning.

Construction of a concrete pressure vessel involves fairly standard civil and mechanical technology already being applied in areas remote from heavy industrial development. Large welded steel vessels with sections of thickness in excess of 3 inches require special manufacture and inspection techniques which are expensive and difficult, especially at a remote site.

Therefore, both economic and technical considerations favour a prestressed concrete pressure vessel.

3.4.2 Internal layout

Several different arrangements of the reactor core, boiler units, and circulators have been investigated. The arrangement giving minimum cost is shown in Figure 1. Although the heights of the pressure vessel and reactor building are both much greater than with the more squat arrangements with the boilers placed around the core, the overall cost of the pressure vessel internal shielding and reactor building is much less. Also, placing the boilers above the core ensures adequate natural circulation of coolant at normal working pressure for removal of fission product heat from the core in the event of complete loss of forced circulation.

All components within the pressure vessel are supported by the lower end slab. This simplifies design and construction of the pressure vessel liner and assembly of components within the vessel.

3.4.3 Pressure vessel design

Details of the concrete structure and cable arrangement(5) are shown in Figure 8. The design philosophy is similar to that used for vessels being constructed overseas.

The design of the vessel is based on the concept of an ultimate pressure at which the pre-stressing cables would fail systematically. The ultimate pressure has been chosen to be 2750 p.s.i., i.e. 2.75 times the working pressure. The pre-stressing cables are loaded to nominally 60 per cent. of their minimum ultimate strength. Under the conditions of full pre-stress and zero pressure, the maximum design stress in the concrete is 2500 p.s.i. reducing to 470 p.s.i. at working pressure. The minimum compressive strength of the concrete at 28 days is assumed to be 6000 p.s.i. with a corresponding tensile strength of 600 p.s.i. The pressure producing the condition of zero pre-stress is 1230 p.s.i. The pressure at which the concrete would be expected to crack is approximately 1520 p.s.i.g. under test conditions and 1280 p.s.i.g. under combined pressure and thermal stress loading with the reactor operating at rated power. The safety valve relief pressure is 1100 p.s.i.g. and the test pressure, 1265 p.s.i.g.

The flat end slabs present a difficult design problem because behaviour under load is not, as yet, well understood. In this design, the ultimate failure of the slab is assumed to be a shear failure at the junction with the cylindrical wall at the ultimate pressure.

The detailed behaviour in the vicinity of penetrations is also not well known. In this design, a careful compromise has been made between the condition producing uniform strain throughout the concrete and the condition which causes buckling of the penetration liner. Spacing of penetrations is sufficient to cause no problem in arrangement of pre-stressing cables.

The mild steel liner is designed to satisfy the requirements for adequate resistance to buckling in service and sufficient rigidity during construction of the vessel. This is achieved by choice of thickness at 0.75 in. and attachment of stiffening plates, 6 in. by 1 in. thick in section, which key the liner to the concrete.

The temperature of the concrete outside the liner and surrounding penetrations is kept below 60°C by attachment of laminated stainless steel insulation to the inner surface of the liner and by a fully duplicated network of pipes carrying demineralised cooling water attached directly to the outer surface of the liner. The heat flow through the insulation has been estimated to be 1.4 MW and heat deposition in the concrete by absorption of gamma radiation 0.2 MW. The maximum gamma energy absorption density in the concrete is estimated to be 0.25 mW/cm^3. Details of the vessel cooling system and associated plant(5) are shown in Figure 9.

3.5 Boilers and Power Plant

3.5.1 Choice of boiler design

The "once-through" Benson type boiler was chosen because its capital cost is lower than that of recirculating type boilers when enclosed in a pre-stressed concrete pressure vessel. A final assessment, taking into account relative difficulty with control of the boiler and the need for high feed water purity, may finally favour the recirculating type boiler for this application.

The choice of steam conditions is not restricted by the reactor primary coolant temperatures and therefore it was decided to specify the most advanced steam conditions at present in use in plants of this size. Supercritical conditions were rejected in spite of higher cycle efficiency, because overall plant and operating cost is, at present, greater than that of the best sub-critical plant. Also, at the considerably higher pressure, water leakage would almost certainly be greater, and reliability of the tubing somewhat reduced. The cycle chosen is, therefore, the 1050°F (565°C), 2400 p.s.i. reheat cycle in common use in plants with unit size 200 MWe and above.

The choice of six boiler units was dictated by optimum use of the area available between the core and pressure vessel wall. The boiler dimensions do not determine pressure vessel diameter which is kept at the minimum required to contain the reactor core assembly to ensure minimum pressure vessel cost.

3.5.2 Boiler design

The design of the boiler is based on a similar design prepared by Messrs. Babcock and Wilcox Ltd. under contract to the A.A.E.C.(46).

The heat transfer diagram is shown in Figure 10 and the arrangement of the sections of the boiler is shown in Figure 11. The arrangement of the tube sections is aimed at achieving maximum stability in the boiling region and minimum movement of the steam/water "interface" under transient conditions. The design is close to optimum as the arrangement of the boiler sections takes advantage of the compromise between low tube temperature and the size of tube bank required.

The location of the six boiler units within the reactor pressure vessel is shown in Figure 1. Figure 11 is a general assembly of a single boiler unit(6). An important feature is the location of all tube bank connections above the boiler unit. This permits free differential expansion between the tube banks and the boiler ducting which is internally insulated by laminated stainless steel.

This eliminates the need for bellows or other similar seals on tube penetrations through the ducting but requires an insulated space within the boiler ducting for return of connecting tubework.

The boiler connection tubing is grouped into insulated and cooled penetrations through the pressure vessel wall, the outer ends forming both tube plate and connection headers.

Each boiler unit is protected by relief valves on the pipework connecting the boiler unit to the ring mains located in the base of the reactor building (Figures 2a, 2b, and 2c).

3.5.3 Boiler start-up plant

The main components of the boiler start-up plant are the flash vessel and the dump condenser. As the reactor power is raised, steam produced by the boiler is separated from the feed water in the flash vessel and then condensed in the dump condenser. The boiler pressure is set at 1500 p.s.i.a. at start-up rising to approximately 1900 p.s.i.a. at 25 per cent. Maximum Continuous Rating (M.C.R.) at which point steam may be passed directly to the turbine.

The start-up plant may be used to reject up to 30 per cent. M.C.R., allowing the reactor to remain in operation when isolated from load. For a complete shutdown, the start-up plant serves to reject fission product heat. To guarantee feed water flow for this purpose, three emergency feed pumps are provided. Adequate feed water storage for rejection of heat direct to atmosphere for a period of 1 day allows for failure of the fuel dump container system.

3.5.4 Boiler control

The boiler control system is described in Section 4.8.4 and shown schematically in Figure 12.

3.5.5 Power plant

The power plant is "conventional" in terms of the most modern non–critical steam plant in use in fossil–fuelled power stations. Seven stages of feed heating with split feed pumping, that is, booster and main feed pumps, has been adopted although the present trend is to single stage feed pumping with the development of reliable high pressure feed heaters. All feed pumps are electrically driven. There are three booster and three main pumps. Any two of each type are required for normal operation so there is 50 per cent. standby capacity.

Control of feed water purity is particularly important with a once–through boiler. A somewhat larger than normal treatment and storage capacity combined with continuous monitoring is considered adequate for reactor plant cost estimation purposes.

3.6 Reactor Coolant Circulators

3.6.1 Choice of location and number of circulators

The circulators are located below core level(7), as shown in Figure 1 to simplify the arrangement of internal ducting, and reduce the pressure difference across duct walls. Some neutron shielding is required at the inner end of the circulator penetrations, but this is not a serious disadvantage as space is available and the cost is small.

The circulators are located at the outer end of the penetrations to simplify the design, by eliminating long shafting, and to simplify handling during maintenance. The pressure drop in the 14 ft. long concentric ducting is not significant.

The choice of the number of circulators was made on three considerations:

(1) It is desirable to have the smallest possible penetration size to reduce the rate of release of coolant to a minimum following a penetration failure.

(2) A reasonable redundancy of circulators is required to guarantee adequate forced circulation following a primary circuit depressurization accident.

(3) The number of circulators is restricted by increase in cost and complexity of the circulator system as the number of units is increased.

The optimum number of circulators is six.

The circulator is required to generate a pressure rise of 9.7 p.s.i. which is well within the capacity of a single stage axial flow machine at the design pressure of 1000 p.s.i.g. and impeller speed of 3000 r.p.m. Details of the design are shown in Figure 13.

The significant design feature is the enclosure of the impeller, drive motor and pony motor within a pressure bell sealed to the reactor pressure circuit. While this requires "built–in" cooling and thermal insulation for the motor and necessitates a reliable pressure–balanced lubrication system for the bearings, it overcomes the difficult design and operational problems associated with a rotating seal. Replacement of the oil–lubricated bearings by gas bearings, if practically possible, would improve the design.

The drive and pony motors are of A.C. squirrel cage induction type. This eliminates potentially unreliable commutation and slip–ring equipment but necessitates an external variable frequency A.C. supply to obtain a continuous 10 to 100 per cent. range of variation of speed below the full power speed for flow control. The drive motor is two–pole to give a rated speed of 2940 r.p.m. at mains frequency (50 c/s). The proposed variable frequency supply is provided by four units consisting of a constant speed A.C. motor coupled to a variable speed alternator by a hydraulic speed variator. Three of the four units would normally be in operation giving some insurance against failure, and, in addition, the circulators may be operated at full speed from the reactor standby electrical supply.

3.7 Fuel Handling and Storage

A unique feature of the pebble bed reactor concept is the ease with which continuous on–load fuel handling may be achieved. The fuel handling system may be designed to recirculate the fuel a number of times during its life in the reactor core. It is assumed that the fuel is recirculated an average of 5 times.

While fuel is outside the reactor core during recirculation, it may be:

(1) Annealed to remove partially the effects of irradiation damage, and, therefore, extend irradiation life. (Hilditch et al. 1966).

(2) Checked for fission product release to enable faulty fuel to be rejected.

(3) Checked for mechanical integrity, to eliminate faulty fuel, dust, and particles associated with failure of fuel.

(4) Checked for level of burnup to assess the future of the fuel within a prescribed fuel management programme aimed at optimizing nuclear, thermal, and mechanical performance of the core.

The fuel handling system[8], has been designed to include all recirculation facilities. A schematic diagram is shown in Figure 14 and the physical layout of the plant in Figure 15.

The fuel enters the core through 9 inlet devices and leaves through 7 extract ports each placed at the bottom of a 120° included angle cone in the core support grate. The number of inlet devices may be varied, fairly freely, to suit a particular fuel management scheme. The number of extract ports is much less variable as the cost of an extract port is considerable, and the influence of the relationship between core size and the number and distribution of extract ports is significant to the mechanics of ball flow through the core.

On leaving the core, the fuel is first checked for mechanical defects (for example, chipping, fragmentation, etc.) and defective balls and detritus separated and stored. The fuel is then elevated, pneumatically, to a shielded facility, above reactor core level, in which the fuel flows under gravity through devices which measure burnup, detect release of fission products, and anneal irradiation damage. Fuel balls are rejected via a gas–lock either when they achieve a predetermined maximum burnup, which would be somewhat less than the design maximum burnup, or when they release fission products excessively.

New fuel is added after the annealing furnace and the fuel then enters the core via the inlet devices which are designed to preheat the balls before entry to the core. The point of entry of a fuel ball into the core would be determined by its burnup state within the fuel management programme.

The fuel recirculation system is designed to operate at reactor coolant pressure with pressure locks at all entry and exit points. Cooling of fuel balls while in the system is achieved by return flow of reactor coolant from the coolant purification plant to the reactor primary circuit.

The seven emergency storage tanks beneath the reactor are designed to accommodate the entire core. The fuel while in the tanks is cooled by natural convection and conduction to external water jackets. Emergency unload is achieved in 10 hours using the normal fuel extract devices operated at high speed. Fixed neutron absorbers are placed within the storage tanks to prevent criticality.

Spent fuel, damaged fuel, and particulate matter are stored in seal–welded stainless steel cans of sufficiently small size (30 litres) to allow cooling by direct heat transfer to the "still" air in transit facilities prior to storage in the ponds shown in Figure 3. The pond storage is adequate for the 25 year reactor life if no fuel is reprocessed for recycling.

Fuel handling devices of particular interest are the extract device, the inlet and preheating device, the damaged fuel separator, and the burnup meter.

Figure 16 gives details of a proposed extract device which uses the principle of the rotating disc. A feature of this design is the avoidance of any tendency to shear fuel balls. Experimental testing of this device has shown it capable of handling fuel balls at rates up to 300 per minute.

A design proposal for the inlet and preheating device[9] is shown in Figure 17. The device consists of a tube containing a double column of balls heated, to approximately the temperature they will experience on entry to the core, by a regulated flow of coolant drawn from above the core. Single fuel balls are released from the lower end of the tube by a simple linear motion. As the device is normally always full of balls, the free fall height involved in entry to the core is kept to the clearance between the core and the lower end of the device (2 ft). Some degree of relaxation of thermal stress in fuel balls may occur prior to their entry into the core resulting in a degree of pre-stressing. Although this design minimizes the number and simplifies the nature of relative motions at high temperature, a thorough development programme would be required to prove this device under simulated reactor conditions before use in a reactor.

The design of the damaged fuel element separator[10] has proved to be a difficult problem, mainly because of difficulty in defining the likely nature of damage. The design shown in Figure 18 may be satisfactory if the dimension of damage regions in the diametral direction is significantly greater than the difference in diameter of fuel balls as supplied.

The burnup meter has not been designed[11], but work to date shows that a design based on high discrimination gamma spectroscopy may be the cheapest and most suitable. The level of unique energy radiation from certain fission products (Ce137, Ce144), and Pa233, combined with gamma ray diffraction techniques, may make feasible the use of lithium drift or similar detectors.

3.8 Reactor Control and Instrumentation

3.8.1 General

Movable control absorbers, assumed to control 1 per cent reactivity, are used in an independent loop from the main power demand control loop to maintain required coolant temperature at core outlet. The actual required reactivity investment in movable control absorbers can only be assessed when the value of the temperature coefficient is finally determined. A negative temperature coefficient has been an assumed design condition of this part of the study.

The reactor shutdown system is independent of the normal control system. The shutdown philosophy has been to provide sufficient reactivity worth in shutdown absorbers to obtain shutdown of the reactor core from its operating condition to subcritical at its most reactive condition. Allowance is made for failure of the two shutdown absorbers of greatest combined reactivity worth. Shutdown of the equilibrium core only was considered as no details are available on performance during approach to equilibrium. Section 4.8 gives details of reactivity effects and control and shutdown absorber worth.

3.8.2 Shutdown absorbers and mechanics

The thirteen shutdown absorbers, are located as shown in Figure 19. The absorbers are hollow stainless steel clad enriched boron carbide cylinders of 6 inch outside diameter.

The absorbers are contained within fixed, but replaceable, guide tubes located within the pebble bed core. The conditions to be met by the guide tubes are particularly onerous as absorption of neutrons should be as low as possible and resistance to high temperature environment sufficient to give a reliable life of at least 5 years. The design employs a low neutron capture cross section zirconium alloy inner tube which is adequately robust to form a reliable guide for the absorber. A flow of coolant between a thin stainless steel outer sheath and the tube is adequate to maintain the temperature of the inner zirconium alloy tube below 450°C at the position of maximum core temperature. A number of zirconium alloys containing copper and/or niobium appear to be suitable for the proposed duty.

The guide tubes contain a stainless steel clad beryllia spine which serves two purposes:

(1) Increase of reactivity worth of absorbers by local thermalization of neutrons.

(2) Decrease of neutron streaming from the empty absorber guide tubes when the reactor is operating.

The absorbers are retracted from the core by a "winch" type mechanism(12) located on the upper surface of the concrete pressure vessel, as shown in Figures 1 and 19. Insertion into the core is by free fall under gravity, a disc brake being used to decelerate the rod at the end of travel. An emergency snubber would be provided to arrest the rod in the event of failure of the normal braking system. The absorbers are located immediately above the hot plenum chamber in the fully retracted position. No provision has been made for partial insertion of the shutdown absorbers.

3.8.3 Control absorbers and mechanisms

The six control absorbers are located, as shown in Figure 1, beneath the core and enter the core for a distance of 4 feet as shown in Figure 20.

The absorber is a hollow cylinder of 4 inch outer diameter and is composed of stainless steel clad enriched boron carbide. The space within the absorber is filled with beryllia to increase reactivity worth, the beryllia moving with the absorber rod in this case.

The absorbers enter guide tubes similar in design to those of the shutdown absorbers but in this case under much less onerous conditions as they only partially enter the low temperature end of the core. The use of partial insertion of absorbers simplifies the actuator design and also makes maintenance possible in the confined space beneath the reactor. The actuator is designed to insert and remove rods at a slow but positively controlled rate.

All shutdown and control rods are placed in a position to cause least interference to the flow of pebbles, that is, only vertical motion of the pebbles occurs adjacent to the rods.

3.8.4 Reactor instrumentation

A feature of the pebble bed reactor core is the restriction imposed by the random nature of the core on measurement of operating conditions within the core. Measurement of fuel power, fuel temperature and coolant flow and temperature distributions is not possible within the core. Fortunately, heat transfer coefficients are high and this results in fuel temperatures being reasonably close to coolant temperature. As maximum fuel and coolant temperatures occur at the top of the core, measurement of coolant temperature at core outlet should give an indication, within say 100 °C, of the maximum fuel temperature.

Measurement of coolant temperature at other points throughout the circuit is assumed to differ little from similar measurement techniques in existing reactors.

The power output of the reactor is assessed by two means:

(1) Neutron flux measurements using ion chambers located immediately behind the concrete pressure vessel liner at mid–core height as shown in Figure 8.

(2) Thermal balance taken using coolant temperature and flow measurements.

Monitoring of the temperature of components within the reactor is essential to plant safety, particularly during the early stage of operation. The temperature of the core and hot plenum chamber liners, shutdown and control rod guide tubes, reflectors, pressure vessel liner, fuel handling components, and boiler components could be monitored at sufficient positions to guard against localized excessive temperature. Temperature measurements actuate warning and reactor trip circuits as appropriate.

A constant monitor of ball flow into and out of the core gives core fuel ball inventory. Detection of an abnormal upper surface profile of the core would in the unlikely event of it being required, prove difficult. A possible method would involve the use of a number of gamma detectors viewing the upper surface of the core from a number of different directions.

3.9 Reactor Containment

3.9.1 Containment philosophy

From the commencement of the study it has been assumed that the degree of fission product retention within the fuel elements, under normal operating conditions, is adequate to permit direct release of the reactor coolant to atmosphere following a failure of the primary circuit. This has not yet been proved by experiment and it is more difficult to guarantee a similar high degree of fission product retention following a primary circuit failure. Although the design of the emergency coolant circulation system is adequate, it is not possible to guarantee operation soon after a primary circuit failure. There is, therefore, a definite possibility that the fuel temperature might rise to above 1600 °C resulting in significant release of fission products.

Thus the containment philosophy is to release to atmosphere the initial pressure surge, following a primary circuit failure, and then seal the low pressure reactor building. This system is commonly known as a "vented" containment.

3.9.2 Fission product retention

Calculations(13) indicate the degree of retention of selected isotopes required to ensure that if a depressurization accident occurred the release to atmosphere would be below the recommended permissible levels. Since there is a different permissible level for each isotope it was necessary to consider the proportions likely to be released from the fuel elements. The assumed proportional release was based on measured diffusion rates (Whitfield and Palmer, and de Bruin, private communications). An improvement in actual diffusion rates of 3 orders of magnitude was then assumed and the ratios of the release of fission products to the total fission products produced, or release parameters (R/B values), were calculated for 1000 °C. These are shown in column 1 of Table 1.

TABLE 1

FISSION PRODUCT RELEASE PARAMETERS

Isotope	1 Calculated Release Parameter at 1000 °C	2 Recently Measured Release R/B Values at 900 °C and Burnup of 5×10^{19} fissions/cm³	3 Total Released Activity in Equilibrium in Primary Circuit (curies)	4 Suggested Allowable Accidental Release to Atmosphere (curies)
Kr 85m	1.6×10^{-6}	$(5-650) \times 10^{-6}$		
Kr 85	2.3×10^{-4}	–		
Kr 87	8.9×10^{-7}	$(4-8) \times 10^{-5}$		
Kr 88	1.3×10^{-6}	–		
Xe 133m	5.7×10^{-6}	Not detected	343.45	10^4
Xe 133	8.6×10^{-6}	$(4-1000) \times 10^{-6}$		
Xe 135m	4.0×10^{-7}	Not detected		
Xe 135	2.4×10^{-6}	$(4-500) \times 10^{-6}$		
I 131	1.0×10^{-5}	–		
I 132	1.2×10^{-6}	–	205.53	10^2
Te 132	6.8×10^{-6}	–		
Sr 89	2.9×10^{-5}	–	117.65	–
Sr 90	2.8×10^{-4}	–	47.33	10
Cs 137	2.8×10^{-4}	–	133.97	10^3

The actual number of curies present in equilibrium in the primary circuit from a whole core for each of the element groups is shown in column 3. They present the equilibrium value of the released isotope in the coolant compared with the equilibrium value of activity retained in the fuel elements, with an allowance for 0.1 per cent, of broken balls with R/B values of 10^{-2} above 1000 °C and 10^{-3} below. If a decontamination factor of 2 is allowed for the iodine and an 80 per cent. plate–out is allowed for strontium, the suggested allowable release can be adhered to.

The following points should also be noted:

In arriving at the total released activity, account has been taken of:

(i) Fuel temperature distribution in the core.

(ii) Recirculation of fuel through the core.

(iii) Natural decay but not loss by burnout in the neutron flux.

(vi) Accumulation of long–lived fission products over 25 years with no allowance for removal from the reactor coolant other than an assumed 80 per cent. plate–out of "solid" isotopes.

The measured release parameters have been determined using the fuel material available in 1966. (Private communication G. Hanna).

The figures for suggested allowable accidental release to atmosphere(14) apply to release over a short period following an accident for a typical site reasonably remote from centres of population.

3.9.3 Containment design

The main component of the containment system is the welded steel reactor building enclosing the reactor, the entire primary circuit, and the majority of reactor ancillary plant. Within the steel building and enclosing the primary circuit is a concrete shell which serves to protect the main containment building from missiles and direct impingement of hot reactor primary coolant following a primary circuit failure. It also serves to isolate leakage of reactor primary coolant from the personnel areas of the reactor building during normal operation.

The inner concrete shell is connected directly to the exhaust stack so that exhaust under both normal and accident conditions is via the inner concrete shell. The pressure in the inner shell is always very near that in the reactor building as there is a direct connection between the two volumes.

Following the assumed most drastic failure of the primary circuit, failure of the largest penetration, the pressure in the reactor building would rise to a maximum of 5 p.s.i.g. and then progressively fall to near atmospheric pressure. The welded steel building is, therefore, designed for a pressure of 5 p.s.i.g. which is considerably lower than the 25 p.s.i.g. that would be experienced if it were completely sealed. No consideration has been given to a gross failure of the pre–stressed concrete primary circuit structure as it is considered not to be a credible accident.

Details of the containment design are shown in Figures 2a, 2b, and 2c and also schematically in Figure 21.

Very soon after a failure of the reactor primary circuit, the pressure in the containment building would reach a value sufficient to rupture the diaphragm in parallel with the normal exhaust plant. On completion of depressurization, as measured by pressure gauges, the building would be immediately sealed by louvre valves and the water seals then flooded. The exhaust louvre valves could then be opened against the contingency of a subsequent secondary circuit failure which would be vented by "blowing" the water seals. A non–return valve prevents back–flow through the ventilation inlet system during depressurization.

With the containment sealed, the atmosphere within the containment may then be progressively decontaminated by circulation through the activated charcoal beds and absolute filters of the permanently installed accident cleanup plant. On completion of this operation, and if it is safe to do so the containment may then be slowly vented to atmosphere via the charcoal beds and filters of the accident exhaust plant. Personnel access to the containment building should then be possible; provided the level of radiation from "plated–out" active materials is acceptably low.

Under normal operating conditions, air is passed through the building and exhausted via absolute filters to the stack. Air conditioning is achieved by recirculation within the building through space conditioners.

3.10 Reactor Ancillary Plant

3.10.1 Coolant purification and storage

The reactor coolant (CO_2) purification and storage system, shown schematically in Figure 22, is designed to provide adequate coolant make–up under all feasible operating conditions, to remove water vapour and oxygen continuously, and control the level of carbon monoxide. The plant has not been designed specifically to remove solid, volatile, or gaseous fission products, but is capable of efficiently doing so, up to the point at which special procedures are required to remove fission product heat from filters and absorption beds.

The purification plant, shown in Figure 23, is designed to maintain water content in the coolant below 30 p.p.m. with a leakage rate from all six boiler units of 16 lb/day. The coolant, after purification, is returned to the reactor circuit via the fuel handling system and the coolant circulators, to maintain relatively clean conditions for these removable components.

The reactor coolant blowdown system is designed to cope with the worst overpressure condition, that is, a complete failure of an economizer tube. The primary circuit is protected by 12 relief valves arranged in pairs, exhausting via tube–bundle type heat sinks into six dump tanks. Coolant may be returned from the dump tanks to the reactor circuit or exhausted via activated charcoal absorbers to atmosphere through the containment exhaust stack.

A total of 85 tons of liquid CO_2 may be stored at a pressure of 350 p.s.i.g. and a temperature of $-18\,^{\circ}C$. The reactor primary circuit normally contains 50 tons.

3.10.2 Electrical system

The station electrical system is conventional[15], with the supply to the auxiliaries of each 200 MWe unit available from two sources:

(i) The station transformer connected to the transmission system.

(ii) The unit transformer connected to the main alternator output.

The auxiliary loads are divided at the distribution boards into essential and non–essential loads. The essential loads have a further source of supply from the standby diesel (or gas turbine) generating plant consisting of three 750 kVA units capable of rapid start–up and parallel operation. Connected to the essential supply are:

(i) Emergency boiler feed pumps.

(ii) Emergency cooling water pumps.

(iii) All six of the reactor coolant circulators.

(iv) The majority of automatic valves in the secondary (steam) circuit.

(v) The accident ventilation and cleanup plant.

(vi) Control, instrumentation, and associated batteries.

(vii) Essential lighting, throughout the station.

The continuous total auxiliary and house load has been estimated to be 11 MWe.

3.11 Safety Considerations

In this assessment it is accepted that two important basic assumptions must be realised if the H.T.G.C.R. system is to become a practical proposition.

(i) Retention of fission product and heavy metal atoms within the fuel elements is sufficient to obviate the need for special plant or procedures to control the level of radioactive contamination of the reactor primary coolant. This is an essential economic and operational condition because a highly active coolant circuit would almost certainly be commercially unacceptable.

Consequently, it is assumed that it is safe to release the primary coolant direct to atmosphere on failure of the primary circuit.

(ii) The overall temperature coefficient of reactivity of the reactor is negative at all times. This is very desirable because there is close coupling between power output and the temperature of the fuel and moderator owing to the homogeneous nature of the core.

3.11.1 Safety during normal operation

Accepting the assumption regarding fission product retention within the fuel, no unusual safety problems are envisaged during normal operation other than those usually associated with nuclear power station plant.

3.11.2 Accident studies

The following hypothetical accident conditions were considered.

(a) Accidents at normal pressure

Failure of forced circulation: It was assumed that the reactor would be shut down immediately on detection of failure of forced circulation. The emergency feed water supply and ultimate heat sink described in Section 3.5.5 is adequate to guarantee heat removal from the boiler units. The safety of the plant therefore depends on the reliability of transfer of fission product and photo-neutron heating from the reactor core to the boilers.

For the upflow reference design, circulation of reactor primary coolant by natural convection has been estimated to be 15 per cent. of normal mass flow at operating temperature and pressure. Since the heat output of the core decreases rapidly from approximately 10 per cent. immediately after shutdown it has been demonstrated that natural circulation is adequate for the maintenance of safe fuel temperature.

For downflow reactor designs, it is extremely unlikely that natural circulation would be adequate for safe removal of heat generated in the reactor core after shutdown. Coolant flow reversal may occur resulting in very high fuel temperatures and damage to parts of the coolant circuit normally at low temperature. Thus, an independently guaranteed forced circulation plant would be essential to the safety of a downflow pebble bed power reactor.

Boiler tube failure: Failure of an economizer tube resulting in complete separation of the tube ends is the worst single tube failure, the main effects due to water entering the core being:

- A reactivity addition.
- Corrosion of core and reflectors.
- Deterioration of alloy structural components.
- Thermal stress failure of fuel elements.
- Excessive primary circuit pressure.

The reactivity effect is dealt with in the next section. Corrosion of the fuel and, in particular, the fuel elements at highest temperature, has been estimated to be equivalent to removal of a maximum 0.002 inch of BeO uniformly from the surface of fuel elements per hour if normal temperatures are maintained. Calculations have shown that the temperature of the fuel can be reduced at a rate sufficient to limit corrosion to an acceptable level and at the same time, not produce excessive thermal stress in the fuel surface.

The graphite reflectors are operated in the temperature range 320 °C to 400 °C in which corrosion and production of hydrogen would be negligible. The effect of a high water vapour concentration on non–replaceable alloy components must be assessed carefully because although corrosion would be negligible over the short period of the accident, subsequent metallurgical effects may be significant. The pressure rise caused by a boiler failure would be controlled by the relief valve and dump tank system, described in Section 3.9, which is designed for this, the worst over-pressure condition.

Reactivity accidents: Possible causes of reactivity accidents are:

- Maloperation during removal of control and shutdown absorbers from the core.
- Rapid rate of change of core temperature.
- Addition of steam or water to coolant following a boiler tube failure.
- Excessive core inventory due to inadvertent addition of fresh fuel.
- Rapid change in core geometry.

Details of the control and shutdown systems are given in Section 4.8.

The prevention of maloperation during start–up, and change of output power, is conventional, that is, by control of the maximum rate of removal of individual absorbers and the sequence by which absorbers are removed from the core.

The maximum possible rate of reactivity addition due to failure of the fuel loading mechanism is 0.02 per cent. $\Delta k/k$ per second, which would continue for no more than 1 second. This presents no potential hazard as it is well under the capability of the control system.

Change in core geometry, manifested as a rapid change in average voidage, could cause a dangerous reactivity excursion. This is avoided by design of the coolant circuit and by positively limiting the rate of forced circulation of the coolant to below that required to levitate the core. It has been shown that if the reactor core is placed within a relatively large coolant–filled space (Figure 1) it is buffered against initial surges and high velocities associated with a depressurization accident to the extent that levitation would not occur.

The shutdown absorber worth is designed to allow for shutdown to the cold "unpoisoned" condition assuming a permanently negative coefficient of reactivity. Because of the very large thermal capacity of the core there is no possibility of temperature reduction at a rate sufficient to cause a serious reactivity excursion before the shutdown absorbers can be inserted (insertion time 1.5 secs.).

Allowance has also been made for the maximum reactivity addition due to a boiler tube failure. This has been calculated(16),(17) to be 3.5 per cent. $\Delta k/k$ for the equilibrium reference core. Shutdown is initiated by a measurement of water vapour content at the outlet of each of the boiler units. This is estimated to give a time margin of at least 3 seconds before water vapour enters the core, because of slow release and rate of gas flow in ducts.

The shutdown absorber worth is designed to cover complete removal of the control absorbers from the core (1 per cent. $\Delta k/k$). In assessing the required shutdown absorber worth, allowance has been made for failure of the two absorbers of greatest combined worth to enter the core.

It can therefore be reasonably assumed that adequate safeguards can be provided to prevent a serious reactivity accident with the reference design pebble bed reactor under the equilibrium core condition.

(b) Depressurization accident

The most serious credible failure of the reactor primary coolant circuit is that resulting in depressurization via the largest penetration through the pre-stressed concrete pressure vessel wall. This is, in the case of the reference design, the 3 ft 6 inch diameter circulator penetration. Failure of the concrete vessel structure is considered incredible.

A depressurization accident could result in excessive fuel temperature if accompanied by a complete failure of forced coolant circulation. Figure 24 shows the results of calculations of fuel temperature following a depressurization accident, assuming rapid shutdown of the reactor by a rate-of-coolant-pressure-change signal. Provided that at least three, but preferably four, of the six coolant circulators are brought into operation within 15 minutes of the end of the 1 minute depressurization period, despite the drop in pressure from 1000 to 15 p.s.i., the maximum fuel temperature would be held below 1350°C. It would be desirable, also, to commence unloading the core, at the emergency rate, into the seven water-cooled storage tanks beneath the reactor. This operation which could be completed in 10 hours, guarantees the long-term safety of the system, especially against subsequent failure of forced circulation. Forced circulation is provided by the normal coolant circulators operating at full speed (but greatly reduced power) directly from the essential electrical supply system described in Section 3.10.2. The normal variable speed circulator drive system is not required.

3.11.3 Containment

The containment system, described in Section 3.9.3 is mainly intended as a safeguard against complete failure of coolant circulation following a depressurization accident. If the reliability of the forced circulation is considered adequate and the assumptions concerning fission product release are accepted, then it may be possible to simplify the containment system.

No problems are expected to result from ingress of air into the reactor primary circuit. There may be some limited corrosion of the BeO by contained water vapour but, unlike graphite moderated systems, there is no explosion or fire hazard. The graphite reflectors are thermally isolated from the core and their temperature maintained below 400°C by a flow of coolant in parallel with that through the core. Thus provided that forced circulation is available to the core, there is no risk of a graphite fire. Should all forced circulation fail, the temperature of the graphite would gradually rise leading ultimately to a risk of fire. This would take at least 5 hours, during which time forced circulation could be reasonably restored, and/or the core partially unloaded to storage to reduce the heat source.

Investigation of the coolant velocity in the core following a depressurization accident has shown that under no circumstance of transient flow could the core be disrupted by levitation. Therefore the only depressurization accident which would disrupt the core is failure of a fuel extract penetration. Although this has not been studied in detail it is thought that provision for flooding the space beneath the pressure vessel would be adequate for safe cooling of the fuel.

3.11.4 Summary

Studies of the reference design have not revealed any unnacceptable inherent safety features provided that the assumed negative temperature coefficient and fission product retention within the fuel can be achieved. Failure to achieve either or both of these conditions might be acceptable on safety grounds, but would entail a significant economic penalty because of increased complexity of control, containment, and coolant circuit plant.

The proposal to use a pre-stressed concrete pressure vessel and integral boilers greatly reduces the severity of a loss-of-coolant accident because a major failure of such vessels is considered incredible. This has reduced the demands on the containment, requiring only a relatively inexpensive low pressure building. The compatibility of beryllia in air at high temperature is also an important safety feature of this system under these conditions

The safety studies have enabled certain critical features of the design to be identified; in particular, protection of the core against ingress of water or steam following a boiler tube failure would need careful attention. Also, control of the height of the pebble bed core would pose a difficult in-core instrumentation problem.

4. NUCLEAR ANALYSIS

At the commencement of the study the actual burnup achievable in this system was not known because early in 1964 no computer code was available for carrying out burnup surveys with the constant flux model necessary for calculating burnup in a steady-state equilibrium core.

A preliminary survey was done using the code FEVER (Todt 1962) which operates at constant power and uses four energy groups. One set of four-group cross sections was used in all calculations, this was based on a spectrum derived from a typical composition. Other simplications had to be made because of the limitations of FEVER (no Li6 generation, difficulties in accounting for Be neutron enhancement, and synthetic fission product treatment).

The survey indicated that low moderator-fuel ratios gave higher burnup but the lower moderator-fuel ratios meant longer core life which was limited by radiation damage (see below). The best achievable burnup appeared to result from a fissile:fertile:moderator ratio of 1:16.5:1650. The main objective of the FEVER survey was to produce a relative assessment of different fuel and fertile ratios suitable for use in optimization studies. As a by-product, values of F.I.F.A. of the order of 3.0 were obtained which was very encouraging although it was realized that too much importance should not be attached to these results owing to the unsuitability of the code to our problem, and the inadequacies of the associated data.

While the FEVER survey was in progress a burnup code ORVOP, (Bicevskis et al. 1966) based on the equilibrium core concept, was developed which permitted studies at constant flux, and this incorporated an explicit fission product treatment (Garrison and Roos 1962). In general much lower burnups were indicated by the ORVOP code (less than F.I.F.A. = 1.5). However, it was realized that the cross sections used (based on F.I.F.A. = 2.8) were not calculated for the correct average composition. The ORVOP code also permitted the use of a synthetic fission product treatment (Nephew 1960), and a comparison was made which indicated the limitations of the code FEVER indicated above appeared to account for the differences in ORVOP and FEVER burnup predictions. It was shown that Nephew's treatment would give higher values of F.I.F.A. because it underestimates the buildup of non-saturating fission products. In view of these uncertainties the optimistic values of F.I.F.A. = 2.0 – 3.0 were assumed temporarily as reference data.

In March 1964, Physics Division had commenced the development of a complex of burnup codes (GIBBA). The burnup equations are solved analytically in GIBBA and this is ideally suited for the constant flux model, although the versatility of the complex permits other modes of operation.

The numerical results of GIBBA and ORVOP were cross checked giving good agreement. The GIBBA survey based on Nephew's fission product treatment and preliminary nuclear data indicated that a F.I.F.A. of only 1.75 would be obtainable. This was accepted in early 1965 when the design parameters were being fixed.

Refinement of the fission product treatment and other nuclear data, resulting in the code GYMEA (Pollard and Robinson 1966), showed that a maximum burnup of F.I.F.A. = 1.4 only was possible and it was immediately realized that the open cycle concept was unlikely to be economic.

It should be noted that only the Pu/Th/U233 system has been considered and for the calculations a homogeneous mixture of fissile, fertile, and moderator material was assumed.

4.1 Fuel Cycles

The fuel cycles of interest were:

(i) Open cycle with plutonium feed fuel and discard of used fuel.

(ii) Open cycle with plutonium feed fuel and processing of used fuel to extract U233 for resale to offset initial plutonium costs.

(iii) Closed U233 recycle.

(iv) Closed cycle with re-use of U233 in the system and plutonium make-up.

These cycles have been studied using methods based on the equilibrium core condition. These methods were incorporated into the GYMEA code complex (Pollard and Robinson 1966) and surveys (Bicevskis 1966, Hesse 1966) conducted to determine the best compositions for use in the reference design.

The open cycle (i) is of interest during the start-up and approach to equilibrium phase because the required U233 is not available, but because of restricted burnup (see Section 4.2.1) does not seem to be economic as a long term cycle. The processing of used fuel, (ii), could only be economic if the U233 could be sold at a price exceeding twice the plutonium price and is also not likely to be economic on a long term basis. Although it may be possible to achieve a self-sustaining closed U233 cycle, (iii), with the 200 MWe reference design this could only be done at very low burnup (F.I.F.A. = 0.2–0.4) and possibly reduced power density which would lead to very poor fuel cycle economics. This means that the closed cycle with plutonium make-up, (iv), is of major interest.

Early calculations indicated that in a recirculating system, such as the reference design, there would be some increase in burnup with delays outside the core since the protactinium decay to U233 would increase the effective conversion. Later, more accurate, calculations showed only a small increase and this was confirmed by German (A.V.R.) assessments (Hayes 1963).

4.2 Reactivity and Burnup

4.2.1 Pure plutonium feed

A neutron balance for this system in the reference design for varying fuel composition is shown in Table 2 (Bicevskis 1966). In the 200 MWe reactor the buckling is approximately 2.0×10^{-4} cm^{-2} and maximum burnups of 1.4 appear to be possible with compositions ranging from 1:9:1200 to 1:8:1600 (fissile:fertile:moderator atom ratios). A plutonium composition of 78 per cent. Pu239, 17 per cent. Pu240, and 5 per cent. Pu241 was used (3000 MWd/tonne) in all studies and it should be noted that plutonium with a high proportion of Pu240 and Pu242 from higher burnup fuel (for example 10,000 MWd/tonne) would result in inferior burnup performance in this system.

4.2.2 Uranium 233 fissile feed

Calculations have shown that the higher eta value for U233 fuel leads to much higher maximum burnup of approximately F.I.F.A. = 2.0 and thorium ratios tend to be much higher. However this is a rather hypothetical situation because there is but slight possibility of a source of U233 fuel being available. With reprocessing of spent fuel the variation of the ratio of the amount of U233 and Pa233 recovered to initial fuel supplied (recovery fraction) with varying burnup (Bicevskis 1966) is as shown in Figure 25. The recovery fractions are far superior with U233 than with Pu239 feed fuel and it is theoretically possible to have a closed cycle. In practice however the build-up of higher isotopes would depress the recovery fraction and at most only allow very low burnup and power density operation. The most practical solution would appear to be to use U233 recycle with Pu make-up.

TABLE 2

NEUTRON BALANCE FOR REFERENCE DESIGN (PER CENT. ABSORPTION)

Atomic Densities in 10^{19} atoms cm^{-3}

BeO Atomic Density ≈ 4 x 10^{22} atoms cm^{-3}

Atomic Densities		3	3	3	3	3	2	4	3	3
Atomic Densities	Pu	3	3	3	3	3	2	4	3	3
	Th	20	20	20	10	30	20	20	20	20
F.I.F.A.		1.0	1.4	1.8	1.4	1.4	1.4	1.4	1.4	1.4
BeO Moderator (includes (n, 2n) enhancement		−3.47	−2.78	−1.67	−0.53	−3.33	−2.17	−3.17	−2.79	−2.76
Fuel (U233, U235, Pu239, Pu241)		56.82	52.88	48.23	51.18	51.74	52.04	53.15	53.51	52.31
Fertile (Th232, U234, Pu240)		36.96	34.94	34.64	29.30	39.37	36.59	33.95	34.91	34.97
Fission Products		7.04	10.19	12.38	13.61	8.30	9.01	11.04	9.92	10.38
Protactinium (Pa233)		0.62	0.90	1.41	1.08	0.91	1.17	0.74	0.58	1.22
Special Absorbers (Li6, He3, Pu242)		2.05	3.86	4.98	5.34	2.99	3.36	4.27	3.88	3.87
Total Neutron Flux (10^{14}n $cm^{-2}.sec^{-1}$)		2.28	2.63	3.25	3.68	2.41	2.92	2.46	1.66	3.64
Average B^2 (10^{-4} cm^{-2})		4.12	2.12	−0.336	0.83	1.41	1.49	2.34	2.55	1.73
Fuel Element Power Density W/cm^3 (referred to core)	Average	11.04	10.99	10.94	10.92	10.94	10.95	10.90	7.07	14.98
	Initial	20.19	40.86	76.46	106.1	25.91	39.26	40.45	25.63	57.35
	Exit	6.52	4.63	5.72	3.52	6.12	5.14	4.33	2.98	6.30

4.2.3 Uranium 233 recycle and plutonium make-up

The recovery fractions for this system can be seen in Figure 25 to be also significantly higher than with pure Pu239 feed and it has been shown (Mercer 1966) that the optimum fuel cycle economics are achieved in the reference design at a burnup of F.I.F.A. = 1.0 to 1.4 with this fuel system. This is lower than the maximum achievable burnup since a sharp reduction in recovery fraction as the maximum F.I.F.A. is approached is experienced and the balance between cost of make-up fuel and the cost of fabricating new fuel elements requires a lower burnup.

It has also been found that varying the feed fuel composition while holding burnup at F.I.F.A. = 1.0 has very little effect on the U233 recovery fractions and significant reductions are not apparent until moderator fuel ratios of 1:4000 are approached (Figure 26). The economics of the fuel cycle is quite insensitive to moderator ratio in this situation since although the higher ratios mean a larger consumption of BeO, this cost is countered by the lowered fuel inventory costs. With present cost information (see Section 8.1.2.) the fuel component of cost seems approximately constant over the range of moderator: fuel ratio 1:1200 to 1:2500.

4.3 Temperature Coefficients

It is desirable that the temperature coefficient be negative for safety reasons and of small magnitude to limit shutdown rod requirements (say around -1.0×10^{-5} $\Delta k/k$ per deg C). The original calculations(16) for the reference design gave a coefficient of -0.3×10^{-5} for a 1:16.5:1650 composition and F.I.F.A. = 1.75. The coefficients have been recalculated for the reference design over a range of compositions as a difference from GYMEA criticality calculations at 900 °K and 300 °K (Bicevskis 1966). These results are shown in Tables 3 and 4.

TABLE 3

TEMPERATURE COEFFICIENTS OF REACTIVITY V. FIFA

Atomic Density Pu = 3×10^{19} atoms cm^{-3}, Th = 20×10^{19} atoms cm^{-3}
Core Average Power Density = 11W cm^{-3}

F.I.F.A.	Multiplication Factor, k		k900 − k300	$\frac{k900 - k300}{900 - 300}$
	300 °K	900 °K		
1.0	1.1340	1.1096	−0.0244	-4.1×10^{-5}
1.4	1.0457	1.0614	0.0157	$+2.6 \times 10^{-5}$
1.8	0.9452	0.9886	0.0434	$+7.2 \times 10^{-5}$

TABLE 4

TEMPERATURE REACTIVITY INVESTMENTS

Difference in k (per cent.) for core temperatures of 600 °C and 0 °C

Atomic Density, (10^{19} atoms cm^{-3})		F.I.F.A.			
Pu	Th	1.0	1.2	1.4	1.8
2	20	0.17		5.19	
3	16	−1.97		3.47	
	20	−2.44	−0.86	1.57	4.34
	30	−3.41		−1.24	
4	16	−3.09		2.40	
	20	−3.51		0.31	
	30	−4.37		−2.48	
5	16	−3.67		1.62	
	20			−0.55	

Results for the reference design indicated only slightly negative coefficients but also indicated that the fuel coefficient is very much more negative than the combined fuel and moderator coefficient. This is important from safety aspects with sudden reactivity additions(18). In all cases the temperature coefficient of reactivity gets more positive with increase in burnup and with a decrease in thorium concentration. It also seems that over the range of interest a decrease in moderator ratio tends to make the coefficient more negative. Although hot critical experiments would be necessary to eliminate all doubt, it seems that, within the range of compositions and burnups of interest, a desirable temperature coefficient might be achieved by selection of fuel composition and burnup which would be compatible with other requirements.

4.4 Fuel Management and Variation of Fuel Power Density

Since the performance of the core is limited by the thermal stress in the fuel elements (see Section 5.3) it is desirable to know the variation of power density with burnup and position in the core. For the reference design operating at F.I.F.A. = 1.75 a fresh fuel element would have a power density 2.3 times that of the power density of the average fuel element.

Recycle of the U233 reduces this effect since the proportion of high cross section plutonium in the feed fuel is reduced from 100 per cent. to 10–20 per cent. in the range of F.I.F.A. = 1.0 – 1.4. It can also be reduced by management and shuffling techniques and this has been examined (Hesse 1966) by means of a special fuel management code (FRIZLE). The system that gave the lowest peak fuel power density was the simple one-pass system. This was almost equalled by the "one inner pass plus 6 outer pass" system, that is, passing fuel first through the central feed and outlet devices and then through the outer 6 feed and outlet devices, provided that a low F.I.F.A. (1.0) and high thorium content were assumed.

Although the uniformity of power density can be markedly improved by fuel management, it was found that little improvement in burnup by neutron conservation can be expected since the leakage from the system is already quite small (3.5 per cent.). Early calculations had also indicated that the use of separate fuel and fertile elements could increase burnup, but later calculations have shown this effect to be rather small.

4.5 Accidental Steam Leak

The reference core feed fuel/moderator ratio (1:1650) results in an undermoderated (epithermal) system at F.I.F.A. = 1.75 and increase in moderation will tend to raise reactivity. Steam or water addition to the core will replace CO_2 with H_2O molecules and, since the moderating power of H_2 is extremely high, will tend to raise the reactivity. Calculations(16) based on the reference design parameters indicated that 1 to 2 per cent. increase in reactivity would occur with a likely steam investment (0.002 g/cm^3) but that the maximum possible reactivity addition was very high (> 12 per cent. $\Delta k/k$). Some method of preventing water entering the core in large quantities is necessary since the maximum moderator ratios likely to be used still result in an undermoderated equilibrium core. This seems feasible.

4.6 Irradiation Damage

4.6.1 Unfuelled beryllia

It has been estimated (Pryor and Hickman 1965) that at the reference design temperatures, the neutron dose limitation (microcracking) on the BeO matrix should be less than 1 x 10^{21} nvt (> 1 MeV) but that with successive annealing below this dose, the BeO matrix could be taken to 3 x 10^{21} nvt.

The limits for various compositions without annealing are shown in Table 5 below. The table gives the neutron dose required by various fuel ratio and F.I.F.A. conditions, but the conclusions still refer to the unfuelled BeO matrix.

TABLE 5

NEUTRON DOSE LIMITS FOR VARIOUS COMPOSITIONS (UNANNEALED)

Moderator to Fissile Ratio	Average Time in Years for F.I.F.A.=1.0 with Average Power Density =10W/cm^3	Approx. Dose of Neutrons of Energy > 1 MeV for F.I.F.A. = 1.0	Allowable F.I.F.A. for 1 x 10^{21} Dose of > 1 MeV Neutrons
3000	1.83	5.37 x 10^{20}	1.88
2000	2.75	8.09 x 10^{20}	1.56
1650	3.30	9.70 x 10^{20}	1.25
1000	5.50	1.62 x 10^{21}	0.63

It thus seems that values of F.I.F.A. of greater than 2.0 can be allowed with annealing and values greater than 1.0 without annealing provided the moderator ratio is kept higher than 1500.

4.6.2 Particle effects in fuelled matrix

Inclusion of fuel particles lowers the modulus of rupture of BeO. It was expected that swelling of fuel particles or fission fragment damage might in addition seriously limit the life of the fuel element. Experimental studies (Hilditch et al. 1966) have shown that with coarse (152–211 μ) particles irradiation doses up to 5.5 x 10^{20} nvt (> 1 MeV) have a serious effect on the fuel body but with fine (< 5 μ) particles at temperatures below 600 °C the effect is only slight. Since the strength of unirradiated BeO with fine particles is also superior to that with coarse particles, it seems that from a materials point of view the fine particles are preferable.

4.6.3 Nuclear effects of fuel particle size

Although in fixed core reactors self-shielding effects in Pu240 particles have a significant effect on the nuclear performance of the core, it appears as a secondary effect in a continuously fuelled system under equilibrium conditions. Thus, it may well be the material properties which are of greater importance in the long term, and might require that small (5μ) particles be adopted.

4.7 Approach to Equilibrium

The method of approach to equilibrium is an important factor affecting the fuel cost, as shown by Mercer (1966), since the fuel cost can be reduced by lower fissile inventory and shorter period of approach. An advantage of the pebble-bed reactor over certain other systems is that no movable control absorbers are required for release of reactivity as burnup proceeds. It is desirable that this advantage be preserved during the approach to equilibrium so the following methods are preferable for control of reactivity:

- Variation of fissile/moderator ratio as a whole.
- Variation of the fissile/moderator ratio of groups of fuel within the core, possibly to the extent that some unfuelled moderator balls may be used.
- Use of burnable poisons.
- Use of a combination of fissile materials, for example U235 and Pu.
- Variation in the fissile/fertile ratio over the core as a whole or in particular zones.

The complex problem of the approach to equilibrium is yet to be solved. It is complicated by the requirement to:

(a) Maintain a constant temperature coefficient, negative, and small in magnitude.

(b) Maintain adequate shutdown absorber worth with the absorber design required in the final equilibrium state.

(c) Control the maximum fuel power rating to avoid thermal stress failure.

4.8 Reactor Control

Only control and shutdown of the equilibrium core has been considered.

4.8.1 Movable Control Absorbers

Because the equilibrium state is maintained by fuel management, the movable control absorbers are only required to cope with reactivity changes during change of reactor power output. For short term power changes, in the range 30 to 100 per cent. rated power output, a reactivity investment of 0.4 per cent. $\Delta k/k$ in movable absorbers is adequate for control of reactivity change during the associated xenon transient (refer Figure 27)[19]. The movable absorbers are also required to control the coolant temperature with power change so as to maintain boiler tube design temperature, since a reduction of coolant temperature is required at part load. The method of start–up of the reactor from the cold, unpoisoned state has not been studied in detail but a combination of temperature rise by coolant circulation and use of movable control absorbers should be adequate, provided that the temperature coefficient is negative.

Although the control of the reactor has not been considered in detail, a reactivity investment of 1 per cent. $\Delta k/k$ is considered sufficient.

4.8.2 Shutdown absorber reactivity requirements

The 13 shutdown absorbers, described in Section 3.8, are required to shut the reactor down in the cold, unpoisoned condition from any operational state. This requires that there be sufficient reactivity investment in the rods to cover temperature change, change in xenon and samarium reactivity, and reactivity addition due to a boiler tube failure. In assessing the required rod worth allowance is made for complete withdrawal of the removable control absorbers, failure of the two shutdown absorbers of greatest worth and also a margin for safety to cover uncertainty in estimates of reactivity effects.

The accurate assessment of the temperature coefficient is, at this stage, extremely difficult but as already explained has been attempted (ref. Tables 3 and 4 above). Calculations indicate that the average temperature coefficient for the equilibrium core (at a F.I.F.A. of 1.75) is approximately -0.3×10^{-5} $\Delta k/k$ per deg C for the reference design. This requires absorber worth of 0.2 per cent. $\Delta k/k$ for shutdown to the cold condition.

The estimated xenon reactivity at rated power output is 1.6 per cent. $\Delta k/k$. The reactivity addition for the equilibrium core due to the pressure of steam in the coolant following a boiler tube failure has been estimated to be 1.5 per cent. $\Delta k/k$ rising to a possible maximum of 13 per cent. $\Delta k/k$ with buildup of water vapour concentration. Study of the conditions following a boiler tube failure shows that it is extremely unlikely that the water vapour concentration in the coolant within the core would rise above the value which obtains immediately after the cessation of the water/steam flow from the failed boiler unit. This is because, as the reactor circuit cools, steam would condense and the resultant water would collect in the space beneath the core. It would not collect within the core, as the core is always at a higher temperature than its environment owing to fission product heating. In setting the requirement for absorber worth at 3.5 per cent. $\Delta k/k$ some allowance is therefore made for increase in water vapour concentration within the core, due to formation of fog with cooling.

The required reactivity worth of the shutdown absorbers for the equilibrium and reference design is shown in tabular form in Section (f), of Appendix 1 the total being 6.3 per cent. $\Delta k/k$.

4.8.3 Shutdown absorber reactivity worth

Reactivity worth of control absorbers in an intermediate energy reactor spectrum involves the absorption of epithermal neutrons by the absorbers as well as the thermal neutrons. Initial calculations[20], utilized a model where the rod was considered black at energies less than E_b and transparent at energies above E_b, E_b being determined by blackness theory. Subsequently, the model was improved (Spinks 1965a) to account for the variation of blackness with energy.

Because there is a small number of shutdown rods, widely spaced, they must be treated explicitly in a space dependent calculation. The use of a finite difference rather than perturbation treatment is indicated (Spinks 1965b) but since this cannot represent true control rod shape, the development of a theory to provide a correction was required (Spinks 1965c).

Finite difference diffusion codes, in addition, consume too much computer time when used in three dimensional geometry. It has been discovered, (Spinks 1965d) that the problem of partially inserted control rods can be transformed to a two dimensional problem.

The worth of the 13 Be–moderated control rods[21] is 6.0 per cent. For the 1:16.5:1650 equilibrium core this figure increases to 10.6 per cent.

After allowance has been made for rod failure (2.5 per cent. $\Delta k/k$) and actual requirements (6.3 per cent. $\Delta k/k$) the safety margin is 1.8 per cent. $\Delta k/k$.

With lower burnup the equilibrium core will tend to be less moderated so that the maintenance of control rod worth will require higher moderator ratios in the feed fuel or larger rods.

4.8.4 System control

Analogue computer studies[22] have shown that the reference design plant is not likely to have any unique control features. A schematic diagram of the proposed control system is shown in Figure 12. The system provides adequate control for temperature coefficients in the range $\pm 5 \times 10^{-5}$ $\Delta k/k$ per deg C as shown in Figure 28. Figures 29 and 30 show that spatial instabilities would be unlikely to occur for a core of the reference design size with a power coefficient of magnitude less than 5.6×10^{-5} $\Delta k/k$ per deg C per MW[19].

Thus provided the temperature coefficient is kept small, the system control is unlikely to be difficult.

5. ENGINEERING ANALYSIS

Engineering analysis of the reference design was necessarily based on fairly simple models and tests. For instance the core parametric survey was carried out by means of the code FLOAT[26] which, for simplicity, ignored cross–flow effects. The design of the reference fuel element assumed simple thermo–elastic theory, uniform material properties based on a compilation of average properties of BeO, and various factors to allow for the more complex effects.

A more exact analysis used computer codes such as TASPOP[23] [24] and MULTI (Binns 1966b) which took into account many of the effects ignored in the design analysis and this revealed certain inaccuracies in the original methods. However without the crucial experiments it is not yet possible to be certain of the exact situation.

In the following account the design analysis only is presented, with comments on the likely situation as indicated by the more exact analysis.

5.1 Overall Core Performance

5.1.1 Gas flow and power distributions

Gas flow through a randomly packed cylindrical bed of pebbles is non–uniform. The non–uniformity is caused by spatial variation of both voidage and fine structure power density due to differences in ball packing and overall power density distribution which causes local cross–flow. This should be taken into account for very precise calculations but it has been found that the gas temperature rise due to this effect is small compared with the total average rise. Of the two effects the random voidage variations were far more important. It was also shown that the effect of radial and axial (coarse structure) power shapes on coolant flow distribution and temperature could be adequately defined by using the usual "chopped" cosine power distribution and that maximum stress was little affected by substitution of more realistic core power shapes. Further work using chopped cosine axial power shapes[24] showed that with fixed axial and radial form factors, permissible ball size did not vary significantly with core length to diameter ratio but increase in radial form factor had a considerable effect on limiting the maximum average power density.

The radial variation of power distribution results in lower temperatures at the periphery with high central temperatures. In a randomly packed pebble bed this effect is accentuated by an increase in voidage at the outer walls, due to packing phenomena, which considerably increases the gas flow over 4 – 5 ball diameters from the wall[25]. This effect varies with recirculation of the bed (ageing) as rhombohedral packing is known to form at the edge of the bed with resultant decrease in voidage. The same effect presumably occurs around the control rods and other core penetrations and effectively causes a "bleed" of cold gas through the bed so that the maximum bed temperatures must be raised even further.

This "bleed" effect is difficult to control, or even keep constant, in a pebble bed reactor but may be of advantage in ensuring cooler fuel/reflector or fuel/control–rod interfaces. In this study it has been taken into account in assessing the maximum temperatures.

5.1.2 Fuel element and coolant temperatures (Figure 31)

The maximum average coolant temperature ($\sim$ 1100 °C) (averaged with respect to the fine structure) is limited by the above considerations and the maximum allowable fuel element temperature. The maximum surface temperature would be experienced by a new fully fuelled element within the central core region at a position close to the top of the core and in a close packed array. Since the maximum surface–to–gas temperature drop is small (10–50 °C) the most significant factor would be the gas temperature around the ball rather than power output, and this is, of course, highest at the top in the upflow concept. The results depicted in Figure 31 have been calculated using the code FLOAT[26].

Fission product release is strongly influenced by the temperature of the fuel particle. It does not seem to be restricted significantly by the outer coating of the element. In this case, the temperature variation across the element radius is important since fission product release depends on ball centre temperatures and hence the power density within the ball is also important. Maximum surface to centre temperature is again not large (about 50 °C) so that the highest temperature above the gas temperature reached by any ball is about 100 °C. Since maximum average coolant temperature at core outlet is expected to be about 1100 °C the maximum material temperature[1] will be about 1200 °C (see Appendix 1 Part (b) (iv)). Fission product release at these temperatures may become significant. The seriousness of this situation is mitigated by the fact that less than 2 per cent. of the fuel is above 1100 °C at any one time[13] (Figure 31). The analysis using TASPOP shows that the peak temperatures might be about 100 °C higher than those shown in Figure 31 due to cross–flow and gas bleed effects. (Binns 1966a) and may lead to more serious fission product release.

5.1.3 Levitation effects

One of the major limitations in the reference upflow arrangement is the need to avoid levitation of the top balls. If it occurs, there may be an increase in ball surface wear rate and small fluctuations of reactivity. None of these effects are dangerous in themselves, but at higher gas flow there is the possibility of large "bubbles" of voidage moving through the bed with rather more serious effects.

The first of these phenomena is called levitation and the second bulk levitation. The levitation factor is defined as the ratio of the upward coolant force on the fuel ball over the weight of the fuel ball. The bulk levitation factor is the total upward coolant force on a column of balls over the total weight of the column.

Early experimental work (Sanderson and Porter 1959) showed that in a random bed the top layer of balls began to move (spin and rock) when the gas velocity was such as to produce a levitation factor of 0.85. The gas bleed at the walls has been estimated to worsen the situation by nearly 22 per cent. and a further factor of 0.78 has been included to account for this effect.

Levitation effects may be mitigated by:

(i) Raising the outlet gas average temperature to the limits permitted by the BeO matrix (850 °C) and by–passing excess coolant around the core for subsequent mixing with the core outlet gas, to reduce its temperature to a level suited to the boilers (750 °C).

(ii) Tapering the core so that the top balls experience a lower gas velocity.

(iii) Using a lower length to diameter ratio (0.6).

(iv) Raising the primary circuit pressure.

(v) Using larger diameter balls and lower power densities.

Each of these methods tends to result in slightly higher direct plant or fuel costs but the overall effect is offset by the lower pumping power requirements. The result seems to be a slightly increased cost but a much more technically attractive system and one which can be better predicted without experiment. The use of a larger diameter of balls would involve higher stresses or lower power densities, but this might be automatically offset to some extent by the higher core temper–atures allowing stress relaxation.

Levitation of the balls can also occur through accidental increases in gas flow or gas temperatures. Power transients are limited to 110 per cent. by over–power trips. Over 150 per cent. maximum power would be required to cause levitation; but even if this happened the resultant levitation from the increased gas velocity caused by the increased temperature produced by the power increment could not occur rapidly owing to the large specific heat involved with the BeO fuel dispersant.

A temporary gas flow increase of 12 per cent. could cause levitation but an accidental increase in circulator speed could be prevented by suitable design of protective devices. It might be possible in the event of a hot plenum duct failure but this is very unlikely since vibration fatigue can be designed against and the pressure differential is only 4 p.s.i. There is no other feasible duct failure likely to give 12 per cent. gas flow increase.

5.1.4 Ball flow

A major problem in pebble beds with low length to diameter ratio is the transit time variation factor between the fastest and slowest pebble. In beds with length to diameter ratio less than unity, this factor has been measured to be larger than 20. On the other hand, with a ratio approaching two and core (grate) angles of 35° to the horizontal or greater, a transit time factor of below 2 should be expected (G. Tingate, Private Communication). The optimum conditions have not yet been established but the indications are that by using 7 outlets, to give an effective length to diameter ratio of about 1.8 per outlet and grate angles approaching 45°, the transit time variation factors should be below 2. It may be possible to arrange for the slowest travelling balls to be in a position of low flux so that total irradiation dose is evened out over the bed. For instance the balls at the outer walls must suffer a substantially lower fast flux and thus avoid micro–cracking of the matrix. The thermal flux and hence fission rate may be not so low and damage from fuel swelling may still occur. Since on the other hand the balls will be at lower temperature the effects may be worsened. All this would need to be assessed to get a true picture of how ball flow rates should be varied.

The actual burnup variation could well be much less than 2 and possibly arranged to approach unity by varying outlet rates, outlet positions, and fuel management techniques.

The control rods have been placed in positions where ball flow is vertical so that no cross-flow around obstructions is required. It is expected that the results of the main statistical pebble bed experiment[27] will enable an adequate arrangement to be designed.

5.2 Fuel Element Design

5.2.1 Reference fuel element

A decision to increase the size of the element from 1 inch nominal diameter to 1⅜ inch nominal diameter was made when the upflow levitation problems were considered.

Coating of the fuel element was proposed to protect the fuel from wear or erosion during its passage through the bed. The thickness of this coating was originally determined[28] by optimizing stresses since the stress resistance of the unfuelled matrix (30,000 p.s.i.) was expected to be substantially stronger than the matrix with fuel particles embedded in it (20,000 p.s.i.). The design was intended to produce these stresses at the outer skin and the outer fuelled region respectively in a 1 inch dia. pebble and assumed uniform material properties. This indicated a shell thickness of approx. 0.050 inch. Later work using non-uniform material properties indicated much larger stresses in the outer surface (Binns 1966b) but the actual situation is complicated by the manufacturing processes.

Although the pebble diameter has been changed, the final values of stress resistance have not yet been experimentally determined and the 0.050 inch coating and the original stress increase factor of 1.2 have been retained.

5.2.2 Unbonded shell element

The idea of an unbonded shell element is thought to be practicable and some success has been achieved in producing a small gap between shell and core. The analysis of the stresses produced in a concentric arrangement[29] indicated that, with a 0.150 inch thick shell, core power densities could theoretically be increased by a factor of about 3 without failure.

In the case of the bonded shell, large improvements are also possible by concentrating the fuel in a thin layer fairly close to the surface. In practice, however, some eccentricity of core might be experienced. This situation has been analysed[30] and it has been shown that in the worst case (full eccentricity) an improvement of only about 60 per cent. is possible and this requires very close tolerances on gap size. There is some doubt that any improvement would be possible if full eccentricity applies (Binns 1966b).

There is some uncertainty about just how eccentric the core would become, however, and there are grounds for treating the question statistically, that is, how many balls out of a thousand would have full eccentricity in practice. The only reasonable way to find out would be by some form of in-pile test.

5.2.3 Graphite buffer layer

A design involving use of a graphite buffer layer has been analysed (Binns 1966b) and the optimum thickness of the layer found to be about 1/16 inch. The idea is to isolate the expansion of the fuelled inner core from the shell by the softer graphite layer ($E_{graphite} = 8 \times 10^6$ p.s.i. and $E_{BeO} = 40 \times 10^6$ p.s.i.) and at the same time avoid the eccentricity problems of the unbonded shell. It was found that the stresses could be close to those for the concentric unbonded shell (factor of about 3 at high temperatures) which would again be an immense improvement in performance.

There is also the possibility of including the fuel in the graphite layer so that reprocessing could be greatly simplified by an immediate BeO fuel separation. The most severe problems to be overcome with this element are those of manufacture and the possibility of graphite corrosion and fuel release when an outer shell cracks.

5.3 Fuel Element Stress Analysis

5.3.1 Failure of the element

The actual criterion of failure is difficult to specify without in-pile testing. It is felt that the limitation will be excessive release of fission products due to thermal stress cracking but the relation between these effects is not yet known. It has been assumed tentatively that thermal stress cracking itself must be avoided and an analysis has been attempted on this assumption.

For a fuel element the relation between thermal stress resistance of the material and the actual stress imposed can be most easily represented by the stress parameter relation:

$$qd^2 S_F = \frac{\sigma(1-\mu)\,k}{E\,\alpha} \times I_F \quad \text{watt cm}^{-1}$$

where

q = core power density (watt cm^{-3})

d = element diameter (cm)

σ = surface tensile stress (p.s.i.)

μ = Poisson's ratio

k = thermal conductivity (watt cm^{-1} deg C^{-1})

E = Modulus of elasticity (p.s.i.)

α = thermal coeff. of expansion (deg C^{-1}).

S_F is a factor to allow for the element configuration (in this case spherical) and can also be extended to account for non-uniform surface cooling due to surrounding packing geometry.

I_F is a factor which accounts for the change in properties (particularly k) with temperature; variables introduced in manufacture such as fuel loading, residual stresses, coating thickness, etc; deterioration of properties due to irradiation damage, that is, fast neutron dose (nvt > 1 MeV); and fuel particle effects due to stress concentrations, fission product swelling, and matrix damage from fission fragments.

The situation for an average fuel element is qualitatively depicted in Figure 32b, and the simplification of a once-through upflow core is used to clarify an otherwise complex picture. The upper two full curves represent the variation of material thermal stress resistance properties $\left(\frac{\sigma(1-\mu)\,k}{E\,\alpha} \times I_F\right)$ as the temperature descends and irradiation damage accumulates. The lower of these two curves represents the 99.9 per cent. confidence limit (that is, approx. 1 in 1000) for the worst elements and can be assumed to include manufacturing variations such as fuel loading, coating tolerances, coating stress increase, and residual stresses, all of which would be included in a uniformly surface cooled element in an in-pile nuclear test on actual elements.

The lowest full curve represents the actual stress situation that would be experienced by a uniformly surface cooled element as it travelled through the core. This is derived from a depletion of fuel as it is burnt and a cosine axial neutron flux.

The further complication of non-uniform surface cooling is depicted by the saw-tooth broken curve above this which represents the effect of the different random configurations surrounding the ball as it passes through the bed. The effect has been estimated (Holy 1966) to increase the uniformly surface cooled stresses by a factor varying between 1.15 and 1.55 and it can be seen in the diagram that this has caused the actual stress parameter to exceed the stress resistance parameter once. This means the element depicted has a 1 in 1000 chance of stress cracking failure.

Some stress relaxation advantages are expected because of creep and irradiation growth and this is depicted in the lowest chain dotted curve. This effect could cause a stress reversal towards the end of life which would be accentuated when the power output ceased due to removal from the core or reactor shutdown.

The calculations are made even more difficult by the fact that the flux varies radially.

5.3.2 Stress factors

The actual thermal stress experienced by a spherical fuel element in a randomly packed bed is difficult to determine precisely because of the many complex situations possible. The design method of dealing with the problem was to assume a simple, uniformly cooled, uniformly heated sphere under pure thermo–elastic conditions and provide certain factors to account for accentuation of the stress. Since the situations occur randomly as the bed moves through the core a statistical combination of the factors is necessary.

Using the latest values of properties of BeO dispersion matrixes (Smith 1966) and a 0.050 inch coated 1.375 inch dia. ball of the reference type, with similar design factors to those shown in Table 6 below, a computer calculation gave a permissible average core power density under pure thermo–elastic conditions of only about 4 watts/cm^3 (31). A more pessimistic estimate (Binns 1966b) gave a power density of only 2.36 watts/cm^3. The main differences between these assessments were the assumptions made concerning residual manufacturing stresses and differential expansions which at this stage are impossible to determine separately.

A semi–empirical approach using the factors shown in Table 6 was made using a statistical combination of the factors. The single factor value (column 2) was separated into its likely random component (column 3) and its systematic component (column 4). The random components cannot be simply multiplied but treatment of the factors as variances (using root–mean–square addition of each factor minus one) was considered to give a fairer evaluation. The product of this value and the systematic effects gave a combined stress factor of 6.42[32] which indicated that the allowable average core power density would be about 5 watts/cm^3. The thermal stress resistance parameter of BeO based on the latest measurement of average properties is shown in Figure 32b.

5.3.3 Creep and relaxation

In the upflow design, due to the high temperature environment into which the balls are placed immediately on loading into the core, creep effects will commence to operate as soon as do the applied thermal stresses. In this way the thermal stresses may progressively relax before the worst stress condition is reached about 1/3 of the way down the bed. One assessment (32) indicated a probable drop in stress of about 1/3 which would allow average core power densities of $5\frac{1}{3} - 6\frac{2}{3}$ W/cm^3. The pessimistic estimate (Binns 1966b) indicates only 3.84 W/cm^3 (a 63 per cent. increase). A further major improvement was also considered likely if some form of "pre–creep" were used to induce initial residual stresses in the elements either during manufacture or in the reflector just before entry to the core (33). This was indicated to give a probable further improvement to allow power densities of $6\frac{1}{2} - 9$ W/cm^3. The limitation to these methods was deduced (34) to be tensile stress failure at the ball centre on exit from the reactor or reactor shutdown, due to excessive residual stresses, but this question is still controversial.

TABLE 6

DETAILS OF STRESS FACTORS

1 Item	2 Single Factor Value	3 3 σ Random Effects	4 Systematic Effects	5 Source
(a) Local Power Variation	1.05	1.05		Assumed effect of random tight packing
(b) Local Output (varying fuel quantity)	1.05	1.05		Due to variation in measurements during fabrication
(c) Average Heat Transfer Coefficient Variation	–	–		Temperature effect only
(d) Over–power, Start–up Transients	1.10		1.10	Assumed
(e) Instrumentation Error	1.05	1.05		Assumed
(f) Non–uniform Coolant Flow	–	–		Temperature effect only
(g) Local Close Packing and Array Variations	–	–		Temperature effect only
(h) Material Properties Confidence	2.0	2.0		Materials Division data compilation
(i) Non–uniformity of Surface Cooling of Ball in a Random Bed	1.5	1.15	1.3	Assessed (Holy 1966)
(j) Tolerances on Ball Diameter	1.02	1.02		Assumed
(k) Surface Coating	1.2	1.0	1.2	Estimated (See Section 5.2.1)
(ℓ) Local Output Variation (including fresh fuel to average and form factors)	2.8	1.4	2.0	Calculated
(m) Creep and Relaxation Effects	1.0		1.0	Assumed – (See Section 5.3.3)
(n) Contact Stresses	1.0	1.0	1.0	Assumed – (See Section 5.3.6)
(o) Irradiation Growth Effects	0.9		0.9	Calculated

5.3.4 Fuel management

Calculations (Hesse 1966) show that the radial power form factor and the consequent maximum to mean fuel element power density can be greatly modified by fuel management By using a single pass system and reference fuel, the peak to average power density can be reduced to 2.45 compared with factor (ℓ) in Table 6 of 2.8. Alternatively, by increasing thorium and reducing burnup the factor becomes 2.64 owing to a reduced form factor. Since a statistical approach indicates that the form factor is possibly of more significance than the random fuel variation, this might have more actual effect than indicated If both devices were to be used it would appear that a 20 per cent. improvement might be possible. This would justify some study since it enables average core power densities to approach 11 W/cm^3 in a 1⅞ inch ball, if extreme optimism prevails. This was the figure chosen for the reference design.

5.3.5 Irradiation growth stress

Irradiation growth stress is due to differential temperature annealing effects (Pryor and Hickman 1965) on growth, and counters the thermal stresses. It was calculated to have a substantial effect on stresses[35] [36] and has recently been included in the code TASPOP for precise assessment. It is expected that a further 10 per cent. improvement is possible unless reverse stress limitations restrict it.

5.3.6 Contact stresses

In a pebble bed, the forces between individual balls will produce tensile stresses (Hertzian stresses) near the points of contact. These stresses can be quite high and are under investigation[37]. The superposition of thermal stresses and the effect of rolling shear is not known and difficult to predict. Experiments are under way to determine these effects[38] but they will be ignored in the upflow concept because of the lower contact loads and the likelihood of residual compressive stress due to creep at the maximum contact load position, that is, the bottom of the core They are sufficiently important in the downflow concept, however, to make it difficult to proceed with detailed design.

5.3.7 Particle effects

It is now quite clear that the presence of fuel particles in the BeO matrix causes a marked drop in strength; strength decreases with fuel particle size and concentration. (Rotsey and Veevers 1966). A modulus of rupture for unfuelled BeO of 35,000 p.s.i. dropping to around 25,000 to 15,000 p.s.i. (depending on size of particle inclusions) has been assumed.

5.3.8 Wear, corrosion, adhesion, mass transfer

Some wear must take place on the surface of the balls as they slide over handling equipment and each other. The total distance that a ball moves in contact with other balls is about 50 feet in the reference design bed. They also move some hundreds of feet along pipes and through handling devices, but it is not likely that serious wear would occur under these circumstances.

The effect of irradiation, sudden surface quench, due to contact with cold components, and the rolling and contact stresses, in causing surface deterioration and spalling is, however, unknown and it must be assumed that dust and chip production will occur. This must be allowed for in the primary gas circuit and some form of test under actual or closely simulated conditions seems necessary

Mass transfer has been checked (Nicholson and Cairns 1965) in a practical rig, and by theory, (Stuart and Price 1964). At the pressures under consideration and a water vapour content of 30 p.p.m. the rate of corrosion of BeO in the core has been estimated[39] to be about 2 lb/year.

There is also a possibility of adhesion by either direct sintering or by hydrolysis and neck formation at contact points. Actual trials have shown that ½ inch cylinders can stick together at temperatures above 1200 °C and loads greater than 50 lb but this cannot be expected below these values. Since in an upflow core the highest surface temperatures are unlikely to exceed 1100 °C where loads are small, and only 300 °C – 400 °C at the points of highest load, adhesion is not considered likely.

6. DOWNFLOW CONCEPT

A considerable effort was initially devoted to solving the problems of the downflow arrangement because of the difficulty of avoiding levitation of the fuel elements. The downflow system, however, introduced a considerable problem in the design of the grate because of the loads and high temperatures involved. The best solutions were considered to be:

A "Flowing Ball" Grate in which some unfuelled balls were run into the empty core container to form a bottom reflector. This could then be gradually run out with the fuelled balls to give a more uniform flow and subsequently when irradiation limits were reached could be removed and a new bottom reflector run in. This was investigated [40] and considered to involve complex but workable handling and operational methods. The supporting structure was to be either a steel box section cooled by gas bleed through the section or a pile of larger balls resting on a cooled bottom plate with gas off–take at the sides.

An All Metal Grate (probably 25/20 stainless steel) resting on an internally cooled and externally insulated stainless steel box section [41]. This method involved a nuclear penalty because of poor end reflection but this was computed to be small (1 – 2 per cent. $\Delta k/k$) for the BeO system.

The downflow system had a number of known disadvantages relating to pressure drop and pumping power, but in general it was felt that the system would be slightly cheaper. Unfortunately, there were also some problems which could not be solved on the drawing board; these were:

(i) Contact Stresses in the ball surface by gravitational and gas flow forces on the bed. These forces would be quite large (30 – 60 lb wt.) at the bottom of the bed where balls would be at their highest temperature. The expected Hertzian stresses were 30,000 – 60,000 p.s.i. which would almost certainly cause surface failure and accumulation of flaws.

(ii) Adhesion can occur under high temperature and contact loads and these conditions exist near the bottom of the core.

Optimum circuit pressures in both upflow and downflow are high and have resulted in a change to post–stressed concrete pressure vessels for the primary circuit. The advantages from the safety and construction convenience points of view make this an attractive concept.

In addition to the above the likelihood of a "gas–lock" in the thermal siphon system (Thompson 1964) rendered the downflow circuit somewhat unattractive from the safety aspect. In the event of complete blower failure the upflow system can remove all heat by natural circulation; the downflow system may not. This necessitates the provision of guaranteed supply to the blowers in the downflow system and may significantly increase capital cost.

The final decision in favour of the upflow concept for the reference design was made because a considerably greater amount of the design would be done without the need for expensive experimentation and a better economic assessment was possible. Most of the work is applicable to a downflow design, so if the results of the assessment cause a change of design it can readily be made when the essential experiments are completed. An economic assessment of the downflow system is included in this report on the assumption that the technical problems can be solved.

7. GRAPHITE MODERATOR VERSUS BERYLLIA

In the H.T.G.C. thermal reactor field the two main competing materials for moderator and fuel dispersant are graphite and BeO. Possibly some combination of these materials will produce the best system, but to combine them, one must know the merits and demerits of each material. It is enlightening and easy to compare the German A.V.R. (graphite pebble bed) or Thorium High Temperature Reactor (T.H.T.R.) systems with the BeO–P.B.R. because of the similarities that exist.

The A.V.R. and T.H.T.R. systems propose a cheap, readily available, self lubricating and easily fabricated graphite fuel element. For high temperature operation this requires helium coolant and some care in preventing steam and air ingress because of combustion and explosion hazards.

The BeO system proposes an expensive but superior nuclear material in the hope that the improved burnup of fuel, cheaper coolant (CO_2) and reduced chemical problems will lead to a cheaper system.

(a) Fuel Cycle Costs

Nuclear calculations (GYMEA code) have shown that for the reference design, the replacement of BeO with graphite results in a decrease in F.I.F.A. from 1.4 to 1.0 (approx.) because of increased leakage and elimination of (n, 2n) enhancement. This means a substantial increase in fuel consumption costs. On the other hand, graphite has a very much lower material and fabrication cost than BeO and since BeO costs are approx. 1/3 of total cycle costs (including inventory charges) it is possible that graphite can overcome the burnup deficiency. For example if the overall graphite material and fabrication costs are about 20 per cent. of the overall BeO material and fabrication costs, the fuel cycle costs are almost equal. This may however be somewhat unfair to graphite as it is possible that optimization of the core size will give a considerable gain to graphite.

The primary question seems to be whether the costs of obtaining adequate fission product retentivity in the graphite system by particle coating techniques will increase the fabrication costs to an uneconomic level and whether BeO can be made to retain fission products equally well. Since particle coating has, so far, proved very expensive and since the theoretical ability of BeO to retain fission products has not yet been demonstrated in practice, there is no way at present to complete this comparison. The cost of reprocessing dispersion fuels is not known with certainty. It is believed that BeO would be more difficult to reprocess than graphite.

(b) Capital Costs

If we assume that fission product retentivity is similar and primary circuit clean–up plants are of similar cost, the major difference in plant costs would seem to be only in the primary containment. The graphite systems will require guaranteed containment to prevent air ingress which will lead to some form of double containment while the BeO system will require higher pressure containment to overcome pumping power and levitation problems caused basically by thermal stresses requiring smaller fuel elements. A likely solution to both problems may be the use of pre–stressed concrete pressure vessels and costs are again likely to be very similar in each system.

Steam leakage can be a major safety problem in both systems since it can cause, among other things:

(i) Reactivity increase due to increased moderation in the BeO system with the possibility of an excursion hazard.

(ii) Hydrogen generation by water gas reaction in the graphite system with an explosion hazard, as well as the above excursion hazard.

This again must lead to similar costs in preventing steam leaks and providing safety measures.

Since the total nuclear component costs are a small fraction (~ 30 per cent) of the total, it is fairly clear that there cannot be very large differences in cost and it seems likely that the nuclear component costs will be very similar in any case.

(c) Operating Costs

Staff requirements and fuel handling will be identical. The only significant difference must be in the helium and CO_2 supplies. Assessments of the Advanced Gas–Cooled Reactor have indicated an energy cost difference of approx. 0.01 d/kWh in favour of CO_2 because of helium first cost and leakage problems. There may also be a slight advantage to CO_2 in pumping power but this would be outweighed by thermal stress and ball size effects.

(d) Fuel Utilization

The superior nuclear properties of BeO give some promise of providing better fertile material utilization. It can be easily demonstrated, however, that a self–sustaining cycle, which seems just possible with BeO is still insufficient to gain significant utilization of thorium compared with the natural uranium required to build up a power complex. No significant advantage for BeO is apparent.

(e) Summary

In order to draw a definite conclusion, it will be necessary to optimize the graphite system and to include reprocessing costs since the most likely difference will arise in the fuel cycle costs. The differences are so marginal, however, that very small changes in technology could possibly reverse the situation but presently anticipated development costs would almost certainly militate against use of BeO.

8. ECONOMIC ASSESSMENT

8.1 200 MWe Reference Design

8.1.1 Plant cost

A detailed estimate has been made of the plant cost for a twin 200 MWe upflow reference design P.B.R. reactor power plant. This is contained in Table 7, with a comparative estimate for a twin 200 MWe downflow reactor station. Both the upflow and downflow designs have been optimized using the computer code NUROSYS(43)(44). Details of the costing method and the reactor parameters at optimum are given by Mercer (1966). The main difference between the optimum upflow and downflow designs is in the respective values of 11 W/cm^3 and 15 W/cm^3 for the average core power density.

Table 8 gives details of the derivation of the total plant capital cost by addition of "on–costs" to the plant cost estimate given in Table 7. Details of the treatment of "on–costs" are given by Mercer (1966). Interest during construction is based on a construction period of 4.5 years at an annual interest rate of 5.5 per cent. The cost of the first fuel charge includes fresh fuel storage for 90 days' operation at 100 per cent. load factor and interest payments at 5.5 per cent. p.a. on fuel investment prior to initial full power operation. The sum of the total plant capital cost and the cost of the first fuel charge gives the total capital investment in the station. This gives a unit capital cost, including the first fuel charge, of $A243/kWe and $A228/kWe for the 200 MWe upflow and downflow stations respectively.

8.1.2 Fuel cost

The fuel cost has two components. The capital component includes interest payments on the total fuel investment and amortization of the irrecoverable part of the fuel investment. The consumption component includes the cost of fissile, fertile, and moderator make–up, the cost of operation of fuel recycle and fabrication plant(45) (see Figure 33), transport charges, and storage charges.

Fuel costing has been considered under two conditions. The realistic estimate or "present estimate" uses present day material, plant, and labour costs and reference design performance paramaters optimized using the code NUROSYS. The optimistic estimate is an attempt to determine the absolute minimum energy cost which could be achieved if every component cost were reduced to its minimum reasonable value. Details of the derivation of the two cost conditions are given by Mercer (1966), and a summary is contained in Table 9.

TABLE 7

PLANT COST ESTIMATE FOR OPTIMIZED TWIN 200 MWe UPFLOW AND DOWNFLOW PEBBLE BED REACTOR POWER PLANT

Item	Estimated Cost (Units of $A1000)	
	Upflow	Downflow
1. Graphite Reflector	404	293
2. Concrete Pressure Vessels	5,006	4,077
3. Reactor Internals	1,391	1,532
4. Coolant Circulators	595	1,069
5. Boilers	7,098	7,139
6. Containment Buildings	2,748	2,541
7. Control and Instruments	3,571	3,058
8. Fuel Handling	2,466	2,911
9. Coolant Plant	209	209
10. Reactor Civil Works	3,400	3,400
11. Total Nuclear Plant	26,888	26,220
12. Turbo-alternators and Auxiliaries	10,904	11,000
13. Turbine Hall and Assoc. Buildings	3,896	3,896
14. Control and Instruments	2,220	2,220
15. Electrical System	4,000	4,000
16. Ventilation, Cooling Water, Site Services, etc.	5,000	5,000
17. Construction Plant	1,500	1,500
18. Site Development	500	500
19. Total Conventional Plant	28,020	28,116
20. Total Plant Cost	54,908	54,345

TABLE 8

ESTIMATE OF TOTAL CAPITAL COST OF TWIN 200 MWe
OPTIMIZED UPFLOW AND DOWNFLOW PEBBLE BED REACTOR POWER PLANT

Item	Estimated Cost (Units of $A1000)	
	Upflow	Downflow
1. Total Nuclear Plant	26,888	26,229
2. Contingency	2,689	2,623
3. Nuclear Plant Engineering	7,394	7,213
4. Administration	2,662	2,597
5. Sub-Total	39,633	38,662
6. Total Conventional Plant	28,020	28,116
7. Contingency	1,681	1,687
8. Conventional Plant Engineering	2,673	2,682
9. Administration	2,673	2,682
10. Sub-Total	35,047	35,167
11. Training of Personnel	216	216
12. Commissioning Cost	920	920
13. Sub-Total (5) + (10) + (11) + (12)	75,816	74,965
14. Interest During Construction	7,961	7,871
15. Total Plant Capital Cost (for Amortization)	83,777	82,836
16. Cost of Initial Fuel Charge + 90 days Fresh Fuel Storage (100% L.F.)	12,705	8,073
17. Interest on Fuel Prior to Full Power Operation	699	444
18. Total Fuel Investment	13,404	8,517
19. Total Capital Investment in Station	97,181	91,353
20. Capital Cost per kWe (incl. fuel) $A/kWe	243	228

TABLE 9

OPTIMISTIC AND PRESENT ESTIMATE (REALISTIC) CONDITIONS FOR FUEL CYCLE COSTING

Item	Value	
	Optimistic	Present Estimate
1. BeO Powder \$A/kg	8.0	16.0
2. PuO_2 \$A/g	6.0	10.0
3. ThO_2 \$A/kg	15.0	15.0
4. Fresh Fuel Fabrication \$A/kg BeO	See Figure 33	
5. Spent Fuel Reprocessing and Fabrication \$A/kg BeO	See Figure 33	
6. Power System Size MWe	5,000	
7. Annual Interest Rate %	5.5	
8. Reactor Life Years	25	

Two fuel cycles have been considered:

The closed cycle assumes entry into an equilibrium condition in which U233 is recycled and any deficit in fissile material is made up by plutonium. It has been assumed that the equilibrium condition is achieved after consumption of two plutonium fuelled charges and that the initial charge costs as much as a full core of feed fuel. The fuel cost has been evaluated over two 25 year periods, using present worth costing, that is,all expenditure is normalized to present day costs. The equilibrium core from the first generation is transferred directly to the second generation.

The open or "throw-away" cycle assumes complete rejection of the spent fuel without credit from contained fissile material or moderator material.

The optimum open and closed fuel cycle conditions have been derived by Mercer (1966) and are shown in Table 10.

Mercer (1966) has considered the effect of variation of feed fuel composition, burnup (F.I.F.A.) and average core power density in determining the optimum conditions. The results are briefly as follows.

(i) Variation in fuel composition in the range 1:16.5:1650 to 1:10:2500 (fissile:fertile: moderator atom ratio) has little effect on unit fuel cost because of the decrease in inventory costs being balanced by the increase in the cost of fuel turnover and BeO makeup. If a significant reduction could be made in reprocessing and refabrication costs, the trend would then be to compositions with lower fissile content.

(ii) Study of the effect of varying burnup on closed cycle costs shows that fuel cost is approximately constant in the range F.I.F.A. = 1.0 to F.I.F.A. = 1.4. The reference value of F.I.F.A. has been chosen to be 1.0 to minimize irradiation damage. The trend for 200 MWe units is similar to that for 500 MWe units as shown in Figure 34.

TABLE 10

OPTIMUM FUEL CYCLE CONDITIONS FOR 200 MWe UPFLOW AND DOWNFLOW PEBBLE BED REACTOR

Fissile/fertile/moderator ratio 1:16.5 : 1650

Plutonium composition 83 per cent. fissile

Core aspect ratio 0.60

Item	Value			
	Upflow		Downflow	
	Open Cycle	Closed Cycle	Open Cycle	Closed Cycle
Average Core Power Density W/cm^3	11.0	11.0	15.0	15.0
F.I.F.A.	1.75	1.0	1.75	1.0
Overall Recovery Fraction for U233	–	0.84	–	0.785
Recovery Fraction for BeO	–	0.80	–	0.80
Core Life (yrs)	4.75	3.63	3.50	2.47
Time Outside Reactor in Fuel Cycle	–	0.67	–	0.67

(iii) For the open cycle study, the higher the achievable burnup, the lower is the fuel cost. The nominal value of 1.75 was expected to be the maximum achievable with full advantage taken of fuel management. The present estimate of the maximum value of open cycle F.I.F.A. with reference Pu fuel is 1.4 (Bicevskis 1966).

(iv) Increase in core power density significantly reduces the capital component of fuel cost at the expense of only a slight reduction in overall fissile (U233) recovery fraction. The optimum core power density has been determined by compromise between plant capital and fuel costs using the computer code NUROSYS.

The optimum fuel cost for the optimum 200 MWe upflow and downflow pebble bed reactors are given in Table 11.

TABLE 11

ESTIMATED OPTIMISTIC FUEL COST FOR 200 MWe P.B.R.

Fuel Cost	Energy Cost [cent(Aust)/kWhe]			
	Upflow		Downflow	
	Open Cycle	Closed Cycle	Open Cycle	Closed Cycle
Capital Component	0.028 (0.050)	0.030 (0.055)	0.023 (0.040)	0.025 (0.046)
Consumption Component	0.070 (0.124)	0.069 (0.117)	0.070 (0.126)	0.069 (0.117)
Total – cent (Aust)/kWhe	0.098 (0.174)	0.099 (0.172)	0.093 (0.166)	0.094 (0.163)

"Present estimate" costs are in parenthesis. (See above).

Prediction of fuel and moderator material costs is difficult but some further effort might be devoted to determination of the extent of possible reduction of chemical processing and refabrication costs in the closed cycle. However, investigation has shown that a reduction to 50 per cent. of the optimistic cost assumed in this study would only result in a 0.013 cent (Aust) reduction in the fuel cost per unit of electrical energy generated. This is not sufficiently significant to affect the economic potential of the H.T.G.C.R. concept.

8.1.3 Operating cost

The operating cost includes the salaries of operating staff and maintenance staff, administration overheads, consumable spares and materials, and insurance against nuclear liability. The unit cost, which is assumed to vary only with unit size, has been derived by Mercer (1966). For a twin 200 MWe station the operating cost is 0.083 cent(Aust)/kWhe.

8.1.4 Total energy cost

The unit generating cost for the optimized twin 200 MWe upflow and downflow pebble bed reactor power stations is given in Table 12. In deriving the unit cost, the reactor life is assumed to be 25 years, the interest rate 5.5 per cent. (giving an annual capital charge of 7.5per cent.) and the load factor 0.8.

TABLE 12

ESTIMATED OPTIMISTIC GENERATION COST FOR 200 MWe P.B.R.

Item	Energy Cost [cent(Aust)/kWhe]			
	Upflow		Downflow	
	Open Fuel Cycle	Closed Fuel Cycle	Open Fuel Cycle	Closed Fuel Cycle
Plant Capital	0.224	0.224	0.221	0.221
Fuel	0.098 (0.174)	0.099 (0.172)	0.093 (0.166)	0.094 (0.163)
Operations	0.083	0.083	0.083	0.083
Total Generating Cost – cent(Aust)/kWhe	0.405 (0.481)	0.406 (0.479)	0.397 (0.470)	0.398 (0.467)

8.2 Study of the Effect of Variation of Unit Size

The results of a study by Mercer (1966) are shown in Figure 35. The optimistic and present estimate cost conditions are similar to those used in the 200 MWe reference study with adjustment made for variation of unit size. Only the optimum closed fuel cycle is considered.

The main reason for the difference in energy cost between the upflow and downflow systems is the difference in optimum average core power density as shown in Figure 36. This explains the increasing cost advantage of the downflow system with increase in unit size, because the decreasing power density of the upflow system results in higher fuel inventory (capital) cost. Even so, at 500 MWe unit size the cost difference is only 0.03 cent/kWhe.

8.3 Comparison Between Twin 500 MWe P.B.R. and CANDU Systems

A detailed comparison of twin 500 MWe P.B.R. and CANDU nuclear power stations is given in Figure 34 (Hayes and Mercer 1966).

The energy cost of P.B.R. systems is considered for the open and closed fuel cycles and the upflow and downflow reactor designs. The effect of variation of cost assumptions is shown by the use of shaded areas, the lower, most optimistic, edge of the areas representing the combination of the most optimistic cost conditions that could be presently assumed.

To assess the effect of a decrease in the availability of cheap uranium resources on the CANDU energy costs, the price of U_3O_8 has been raised from the presently accepted value of $8(U.S.)/lb to $14(U.S.)/lb and $20(U.S.)/lb. These are fairly drastic price increases compared with available estimates (Raggatt 1965).

Even so, without a significant technological improvement in the reference design P.B.R. system, which itself assumes an extrapolation of presently accepted design conditions (Mercer 1966), this system does not show sufficient economic advantage over CANDU to warrant serious consideration as a competitor.

9. DISCUSSION

9.1 Technical Feasibility

(a) Provided that a satisfactory fuel element can be developed, the upflow pebble bed reference design appears to be technically feasible. The main requirements of the fuel element are:

(i) That it should withstand thermal stress sufficiently to allow optimum average core power density of 11 W/cm³ using a 1⅜ inch diameter fuel ball. A combination of fuel material development, improvement in fuel element design, and reduction in the ratio of maximum to average fuel power density by fuel management might ultimately achieve this aim but there is no experimental evidence that this is possible.

(ii) That it should retain fission products adequately at temperatures up to approximately 1200 °C during operation. Adequate fission product retention will be achieved when no additional complex and expensive coolant purification and containment plant is required to safeguard the system against fission product release. For example, an acceptable release of iodine 131 into the reference design 200 MWe coolant circuit requires the release to birth ratio for the fuel to be no larger than 10^{-5}.

If these requirements were not fully met the concept would still be technically feasible but the economics would be worse and the concept less desirable. Extension of the reference design to unit sizes up to 500 MWe is also technically feasible without increase of coolant pressure but requires a reduction in average core power density from 11 W/cm³ at 200 MWe to 9 W/cm³ at 500 MWe with some increase in fuel cost.

There are some minor questions concerning the integrity of in–core components but there does not seem to be any technical problem which cannot be solved by development or design compromise in the upflow reference design, provided that the fuel element meets the above requirements.

(b) At the assumed maximum burnup (F.I.F.A. = 1.75) the fuel cost of the open cycle is not very different from the optimum closed cycle fuel cost (that is in the burnup range F.I.F.A. = 1.0 – 1.4). Unfortunately the maximum burnup possible with 3000 MWd/tonne plutonium is only about 1.4 with the optimum feed fuel composition of 1:8:1600 (fuel: fertile: moderator atom ratio). This means that the closed cycle is economically superior if the system size is greater than about 2500 MWe. At F.I.F.A. = 1.0 and fuel: moderator ratio of 1200 the closed cycle has a fuel:fertile ratio of about 1:25 for optimum U233 recovery. However variation of moderator/fuel ratio has only slight effect on the U233 recovery up to about 2500 (see Figure 26). If the higher moderator ratio is chosen (for example, to economise on fuel inventory or improve control), the thorium:fuel ratio must be reduced to retain reactivity in the face of higher thorium cross sections. The technical problems are affected in the closed cycle in the following ways:

(i) The lower burnup reduces irradiation damage.

(ii) The higher thorium ratios reduce fuel particle swelling and help to reduce fission product release and matrix damage.

(iii) Higher moderator ratios would improve the control and shutdown rod reactivity worth. but would require lower thorium ratios.

(iv) The higher thorium ratios and lower burnups seem to provide a substantial negative temperature coefficient. It can be seen (Section 4.3) that for low values of F.I.F.A. the temperature coefficient can increase by an order of magnitude, in which case $\Delta k/k$ required for shutdown would be nearer 2.0 per cent. than 0.2 per cent. This would require larger or more effective control rods. Higher moderator ratios tend to reduce temperature coefficients at the same time as giving greater shutdown rod worth so that a suitable compromise can probably be achieved.

(c) The main problem with the recycle system seems to involve the reprocessing. Because of the refractory nature of BeO the dissolution and the separation of the fuel, fission products, and moderator is quite difficult. In fact, this is one of the most difficult fuels to reprocess that has ever been devised (Cairns, Baillie, Farrell, and May 1966). The radioactivity of the components is high and demands expensive remote handling facilities which are also difficult and expensive to operate. The recovered U233 is also quite gamma–active which means that refabrication involves gamma–shielding as well as the alpha–shielding necessary for the plutonium make–up. An additional reprocessing problem may be introduced by the desirability of using a fine (5 μ) fuel particle dispersion for matrix strength, (there is no evidence that small particle size will adversely affect burnup). This would make recovery even less efficient, if possible at all. Presumably some economic compromise would be necessary between acceptable recovery efficiency and high power density.

(d) The downflow pebble bed design has many unresolved technical problems affecting technical feasibility. In particular, the combination of high temperature with high contact loads between fuel elements and also between the core and the support grate gives rise to the possibility of:

(i) Failure of fuel elements due to contact stress.

(ii) Adhesion of fuel elements.

(iii) Excessive rate of wear of fuel elements and core support structure components.

Also the possibility of coolant flow reversal following failure of forced circulation increases the cost and complexity of reactor internals and the guaranteed forced circulation system.

If satisfactory solutions to these problems could be found, then it is fairly clear that the down–flow system would have an economic advantage over the upflow system as the ever–present problem of levitation is avoided. This is particularly true of larger reactor size in the vicinity of 500 MWe.

(e) The compatibility of beryllia at high temperature (1000 °C) with air is probably the most important technical advantage of this system over similar graphite moderated systems. This is likely to produce some economic advantage due to reduced engineered safeguards but the practical operational advantage of reduced administratively applied safeguards may be of greater importance.

9.2 Economic Evaluation

(a) An economic comparison of the optimized upflow and downflow beryllia pebble bed concepts with a relatively developed heavy water natural uranium system (CANDU), Figure 34, has shown that there is at present little justification for further development of the plutonium fuelled beryllia pebble bed concept on simple economic grounds.

The comparison is based on an estimate for a twin 500 MWe CANDU nuclear power station in Australia. It should be noted that in making the optimistic estimate of the pebble bed system generating cost, a sincere effort has been made to assess the maximum possible economic potential of this system. The optimistic costs must not be regarded as actually achievable.

(b) The potential of the beryllia pebble bed system for utilization of fertile material (thorium) is doubtful although it may be just possible to produce a self–sustaining fuel cycle. When allowance is made for fissile material loss, particularly to control the level of transuranic and transplutonic absorbers, and consideration is given to the need to expand the power system size, it is necessary to have a constant independent supply of fissile material[42].

(c) When practical limitations are applied to the maximum burnup that can be achieved in an open fuel cycle it is clear that the closed fuel cycle would be more economic. The recommended fuel cycle is, therefore, the U233 recycle system at a burnup of approximately F.I.F.A. = 1.0. The choice of feed fuel composition depends on the way in which recycle costs vary with fuel composition and throughput. Should the fuel cost prove fairly independent of moderator ratio as seems likely (Bicevskis, Hesse, and Mercer 1966) then the composition of lowest fissile content would probably be best since the capital investment in fuel inventory is reduced and control and irradiation damage problems become less difficult.

(d) The economic advantage of the downflow concept over the upflow concept becomes more marked if the power density is restricted by material properties of the fuel element, and this would seem to be justification for further investigation of the technical problems of the downflow concept.

9.3 Recommendations for Further Study

In addition to experimental and analytic work, already in progress, the following work is recommended to clarify certain aspects of the study:

(i) Fuel Element Performance: An integral experiment, that is, an experiment in which most of the factors affecting fuel element performance are included, is required, in order to produce information of direct use in engineering studies. This may be produced by a versatile in–pile rig, which simulates required conditions, but, ideally, is best obtained from a small test reactor.

In particular, experimental information is required on the relationship between fuel power density, thermal stress failure, fuel temperature, and, possibly, superimposed contact loading. Further, it is of great interest to know to what extent fission product release is related to thermal and contact stress failure so that a realistic fuel element failure criterion may be established.

(ii) Downflow Pebble Bed Reactor: As experimental information becomes available on the effect of high contact load at high temperature on fuel element performance, further study of the downflow system may be justified because it appears to have some economic advantage over the upflow system especially in large unit sizes.

(iii) Reactor Physics: Further investigation is required of fuel management, particularly during the approach to equilibrium phase of the fuel cycle, to determine in detail both the technical and economic merit of the alternative fuel cycles. In the presently assumed approach, employing two initial fuel charges, no detailed consideration has been given to variation of fuel rating, rejection of part burnt material, reactivity balance, or control problems.

Further analytic investigation of the equilibrium phase of the fuel cycle is also required, in order to optimize nuclear and thermal performance. In particular, the spatial and temporal influence of various fuel management schemes on fuel element rating should be determined. Since U233 recycle is the most promising system it should be the subject of more studies.

It is also recommended that consideration be given to alternative fissile fuel materials. The present study has assumed the feed material to be 3000 MWd/tonne plutonium (for example, from Magnox reactors) but an improved once-through cycle might be achieved with low enrichment uranium because of low fissile cost and improved burnup. It is also advisable that 10,000 MWd/tonne plutonium be considered because the latest natural and low enrichment uranium heavy water moderator systems are expected to achieve a burnup of at least 10,000 MWd/tonne. It is expected that nuclear performance with this fuel would be inferior to that achieved with 3000 MWd/tonne plutonium so it may then be necessary to consider using uranium 235.

(iv) Recycle Plant: The effect of throughput and fuel composition on chemical processing and refabrication cost needs further investigation to permit more accurate technical assessment of closed U233 recycle. However, it has been shown in Section 8.1.3 that reduction of the chemical re-processing cost to as low as 50 per cent. of the present optimistic value has no significant effect on the overall economic potential of the concept. There does not, therefore, seem to be much need for further detailed study of recycle plant directed specifically to cost reduction. However the question of fuel particle size would need to be resolved. It would be necessary to compromise between the cost of the grind-leach stage of reprocessing and the cost of decreased average core power density due to reduced fuel element strength.

10. CONCLUSIONS

(a) The reference six-pass core, open cycle upflow design of pebble bed reactor system can be considered to be technically feasible provided that the required fuel element thermal stress resistance and fission product retentivity can be achieved. The theoretical stress analysis indicates that the fuel element would have excessive surface cracking at the desired average core power density.

(b) The system would be simplified and performance improved by using a single-pass closed fuel cycle operating at a value of F.I.F.A. of 1 to 1.2. In this case the optimum fuel : fertile : moderator atom ratios would be between 1 : 20 : 2000 and 1 : 10 : 2500

(c) The reference upflow design system fuelled with 3000 MWd/tonne plutonium and considered under the most optimistic cost and performance conditions does not appear likely to be economically competitive with the CANDU heavy-water moderated, natural uranium fuelled system or its future developments until uranium prices increase substantially. There seems to be no reason why the reference design should be significantly more economic than similar graphite H.T.G.C.R. systems.

(d) Further theoretical analysis of reactor performance without experimental verification will produce little useful information. In particular it would be desirable to determine the effect of power density on fission product release and whether surface cracking is a realistic criterion of fuel element failure.

(e) If a more precise assessment of the system is desired the following aspects merit further study:

(i) Further investigation of the downflow, one-pass, closed fuel cycle concept; in particular the effect of contact loading on the fuel element and the core support structure.

(ii) Further assessment of fuel management schemes, especially the one-pass system using U233 recycle.

(iii) Investigation of the effect of start-up and approach to the equilibrium fuel cycle on fuel cycle economics.

(iv) Economic assessment of fuel cycles using alternative fuels such as low to medium enrichment natural uranium. This may require a change from the P.B.R. concept to a parallel flow concept to obtain sufficient heterogeneity.

(v) Assessment of the economic potential of power topping devices in conjunction with high temperature gas cooled reactors.

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APPENDIX 1

ESSENTIAL PARAMETERS FOR THE REFERENCE DESIGN

The essential parameters used in producing the reference design are as follows:

(a) OVERALL PLANT PERFORMANCE

Reactor Total Thermal Output	520.0 MW(h)
Heat Loss from Reactor Primary Circuit	1.8 MW(h)
Heat Recovery from Coolant Circulation	1.2 MW(h)
Net Heat Transfer to Steam	519.4 MW(h)
Power Output at Alternator Terminals	217.0 MWe
Electrical Auxiliary Load	11.0 MWe
Station Net Electrical Output	206.0 MWe
Steam Cycle Efficiency	43.1%
Station Overall Efficiency	39.6%

(b) REACTOR CORE

(i) Geometry

Shape	Frustum of Cone
Diameter – Upper	17.36 ft
– Lower	13.06 ft
– Average (Equivalent Cylinder)	15.21 ft
Average Height	9.13 ft
Average Voidage	40%
Number of Outlet Cones	7
Outlet Cone Angle (Included)	120°
Weight of Core	91.5 tonnes
Number of Fuel Elements	1.27×10^6

(ii) Fuel Element

Shape	Spherical
Diameter	1.37 ± 0.03 in.
Fuel-Free Outer Layer Thickness	0.050 ± 0.005 in.
Density – New Fuel Ball	3.14 g/cm^3
– Spent Fuel Ball	3.00 g/cm^3
Concentration of Fuel Particles in Matrix (Mixed Fertile and Fissile)	4.13 vol per cent.
Initial Fuel Composition (metal atom ratio)	
PuO_2 (239, 241)	1
ThO_2 + PuO_2 (240)	16.5
BeO	1650
Reject Fuel Composition (atom ratio)	
PuO_2 (239, 241)	0.016
UO_2 (233)	0.5
ThO_2 + PuO_2 (240)	15.4
BeO	1650
PuO_2 (242)	0.15
Average Burnup (Fissions/Initial Fissile Atom)	1.75

(iii) Reflectors

Material	Reactor Graphite
Thickness	60 cm
Voidage – Radial	5%
– Axial Upper	5%
– Axial Lower	30%
Coolant	CO_2
Maximum Temperature	350°C

APPENDIX 1 (continued)

(iv) Thermal and Mechanical Performance

Average Power Density	11.0 W/cm^3
Heat Output	520 MW(h)
Average Fissile Rating (based on initial fuel)	0.83 MW(h)/kg
Axial Power Form Factor (Cosine)	1.3
Radial Power Form Factor (Cosine)	1.4
Fuel Peak Power Density	60 W/cm^3
Direction of Coolant Flow in Core	Upward
Coolant	CO_2
Coolant Flow – Through Core	790 kg/sec
– Bypass	220 kg/sec
– Total	1010 kg/sec
Coolant Temperature at Core Inlet	322 °C
Bulk Coolant Temperature at Core Outlet	865 °C
Peak Coolant Temperature at Core Outlet	1082 °C
Maximum Normal Surface Temperature of Fuel	1131 °C
In–hand for Transient Temperature Effects	140 °C
Maximum Allowable Surface Temperature of Fuel	1400 °C
Maximum Nominal Centre Temperature of Fuel	1179 °C
Peak Centre Temperature of Fuel	1292 °C
Average Temperature of Fuel in Core	680 °C
Maximum Nominal Fuel Surface Heat Flux	19.4 W/cm^2
Peak Fuel Surface Heat Flux	34.5 W/cm^2
Average Heat Transfer Coefficient	0.192 W/cm^2 deg C
Maximum Reynolds Number	72,590
Average Reynolds Number	45,300
Minimum Reynolds Number	26,000
* Maximum Thermo–Elastic Stress in Fuel	49,600 p.s.i.
Total Upward Coolant Force on Core	137,000 lb
Weight of Core	183,000 lb
Net Downward Force on Core Support	46,000 lb
Maximum Contact Force on Fuel Ball at Bottom of Core – no Coolant Flow	22 lb
Maximum Contact Force on Fuel Ball at Top of Core – no Coolant Flow	0.16 lb
Average Coolant Velocity at Core Outlet	3.7 ft/sec
Maximum Levitation Factor at Top of Core	0.68
Allowable Levitation Factor at Top of Core	0.85
Average Levitation Factor at Bottom of Core	0.90
Percentage of Rated Power to Cause Levitation at Rated Coolant Flow	152%
Percentage of Rated Coolant Flow to Cause Levitation at Rated Power	112%
Percentage of Average Coolant Flow to Cause Levitation at Edge of Core at Rated Power and Coolant Flow	122%

(v) Nuclear Performance (Equilibrium Core)

Average Burnup (Fissions/Initial Fissile Atom)	1.75
Average Fuel Life	5.2 years
Average Fast Neutron Dose (> 1 MeV)	1.7×10^{21} nvt

(continued)

* This stress value should be regarded as pessimistic as no account is taken of the effects of thermal and irradiation creep, differential irradiation growth, improved fuel element design (see Section 5.3), improved performance conditions due to optimum fuel management or prestressing before entry to core by "pre–creep".

APPENDIX 1 (continued)

Average Neutron Flux	2.2×10^{14} n cm^{-2} sec^{-1}
Temperature Coefficient – 300–900 °K	$+0.05 \times 10^{-5}$ $\Delta k/k$ deg C
– 900–1200 °K	-3.4×10^{-5} $\Delta k/k$ deg C
Xenon – Operating (100% Rated Power)	1.6% $\Delta k/k$
– Override	2.2% $\Delta k/k$
Boiler Tube Failure – Initial Reactivity	1.5% $\Delta k/k$
– Maximum Reactivity	13% $\Delta k/k$
Water Concentrations – Initial	0.002 g/cm^3
– For Maximum Reactivity Condition	0.1 g/cm^3

(c) Reactor Coolant Circuit

(i) Pressure Vessel

Type	Post–stressed Concrete
Shape	Slab–ended Cylinder
Internal Diameter	28 ft
Internal Height	72 ft
Wall Thickness	14 ft
End Slab Thickness	21 ft
Diameter of Largest Penetration	3.5 ft
Working Pressure	1000 p.s.i.g.
Design Pressure	1100 p.s.i.g.
Test Pressure	1275 p.s.i.g.
Limiting Pressure (Concrete and Tensile Failure)	1330 p.s.i.g.
Ultimate Pressure (Cable Failure)	2750 p.s.i.g.
Concrete Strength (28 day cube)	6000 p.s.i.
Cable Strength (12–0.6 in. strand)	120 tons
Cable Working Load	69 tons
Heat Loss to Coolant System	1.8 MW(h)
Maximum Temperature of Concrete	60 °C

(ii) Circulators

Circuit Pressure Drop – Core	5.2 p.s.i.
– Boilers	3.3 p.s.i.
– Ducting etc.	1.7 p.s.i.
	10.2 p.s.i.
Natural Circulation Recovery	0.5 p.s.i.
Net Pressure Drop	9.7 p.s.i.
Coolant Temperature at Circulator Inlet	321 °C
Number of Circulators	6
Type	Variable Speed Axial Flow
Coolant Flow per Circulator	376 lb/sec
Shaft Speed	2940 r.p.m.
Shaft Power	300 H.P.
Drive Motor Power (electrical)	240 kWe

(iii) Volume and Mass of Coolant

Volume of "Circulating" Coolant	20,808 ft^3
Average Temperature of Coolant	380 °C

(continued)

APPENDIX 1 (continued)

Volume of Total Coolant	31,387 ft^3
Total Mass of Coolant	50 tons
Coolant Transit Time – Core	1.0 sec
–Hot Ducts	2.7 sec
– Boilers	7.4 sec
– Cold Ducts	17.6 sec
– Cold Plenum	3.4 sec
Total	32.1 sec

(d) Boilers and Power Plant

(i) Boilers

Type	Once–through (Benson Type)
Number of Boiler Units	6
Boiler Unit Dimensions – Width	4.8 ft
– Length	6.0 ft
– Height	33.1 ft
Evaporation Rate (one unit)	247,000 lb/hr
Reheat Rate (one unit)	217,000 lb/hr
Reactor Coolant Temperature	
Boiler Inlet	750 °C
Boiler Outlet	321 °C
Feed Water Temperature	470 °C
Saturation Temperature	670 °C
Superheat Temperature	1055 °F (568 °C)
Reheat Temperature – Inlet	728 °F (387 °C)
– Outlet	1051 °F (565 °C)
Feed Water Pressure	2626 p.s.i.a.
Superheat Pressure	2400 p.s.i.a.
Reheat Pressure – Inlet	625 p.s.i.a.
– Outlet	583 p.s.i.a.
Heat Transferred (one unit)	86.7 MW(h)
Specific Rating (referred to tube bank volume)	2.3 MW(h)/m^3

(ii) Power Plant

Arrangement of Turbine Cylinders	1 H.P., 1 I.P., 2 L.P.
T.S.V. Temperature	1050 °F (565 °C)
H.P. Exhaust Temperature	730 °F (388 °C)
H.P. Exhaust Pressure	649 p.s.i.a.
I.P. Inlet Pressure	575 p.s.i.a.
I.P. Inlet Temperature	1050 °F (565 °C)
Condenser Vacuum	1.5 in Hg
Number of Stages of Feed Heating	7
Steam Cycle Efficiency	43.1%
Feed Pumps	3 x 50%
Feed Pumping Power (100%)	8600 H.P.
Number of Emergency Feed Pumps	3
Emergency Feed Pump Power (Each)	50 H.P.

(e) Fuel Handling

Average Number of Transits of Fuel Elements Through Core	6
Average Transit Time Through Core	316 days
Time Outside Core in Recirculation	3 days

(continued)

APPENDIX 1 (continued)

Number of Fuel Inlet Points	9
Number of Fuel Extract Points	7
Average Fuel Ball Rate per Extract Point	24 per hour
New Fuel Ball Make–up Rate (Total)	28 per hour
Emergency Rate of Fuel Ball Extraction per Extract Point	320 per minute
Approximate Time for Emergency Unload	10 hours
Total Activity of a Fuel Ball –	
Average Irradiation at Shutdown	1328 curies
Average Irradiation, 30 hours cooling	363 curies
Maximum Irradiation at Shutdown	1447 curies
Maximum Irradiation, 30 hours cooling	358 curies

(f) Reactor Control and Instrumentation

(i) Reactivity Effects

* Temperature	0.2% $\Delta k/k$
Xenon	1.6% $\Delta k/k$
Boiler Tube Failure	3.5% $\Delta k/k$
Movable Control Absorbers for Short–term Changes	1.0% $\Delta k/k$
Absorber Reactivity Required for Shutdown	6.3% $\Delta k/k$

(ii) Shutdown Absorbers

Number of Absorbers	13
Gross Reactivity Worth of Absorbers	10.0% $\Delta k/k$
Less Worth of Central and one Other Absorber due to Failure to Enter Core	2.5% $\Delta k/k$
Net Reactivity Worth of Shutdown Absorbers	7.5% $\Delta k/k$
Safety Margin	1.2% $\Delta k/k$
Absorber Dimensions – Length	9.0 ft
– Inside Diameter	5.5 in
– Outside Diameter	6.0 in
Absorber Material	30% enriched B_4C in S.S.
Absorber Weight	200 lb
Insertion Time	1.48 sec
Actuation for Insertion	Free Fall (gravity)

(iii) Movable Control Absorbers

Number of Absorbers	6
Net Reactivity Worth	1% $\Delta k/k$
Absorber Dimensions – Length	4.0 ft
– Inside Diameter	3.5 in
– Outside Diameter	4.0 in
Absorber Material	30% enriched B_4C in S.S.
Position of Actuator	Below Core
Proposed Rate of Movement of Absorber	0.5 ft/min

(continued)

* At 100% power output conditions, but referred to unpoisoned, sub–critical equilibrium core state at 27 °C.

APPENDIX 1 (continued)

(iv) Reactor Instrumentation

Number of Ion Chambers	12
Position	In concrete behind liner of P.C.P.V.
Thermal Neutron Flux	2 to 4 x 10^{10} n cm^{-2} sec^{-1}
Gamma Dose Rate	3 x 10^5 R/hr
Initial Start–up Sources	Neutron source balls
Subsequent Start–up Source	Photo–neutrons

(g) Reactor Containment

Type	Vented Sealed Building
Leak Rate (% contained volume per day at 1 p.s.i.g.)	0.1% day
Containment Volume	1.7 x 10^6 ft^3
Exhaust Stack – Diameter	12 ft
– Height	200 ft
Maximum Pressure in Containment	5.4 p.s.i.g.
Time for Completion of Venting	Approximately 1 min

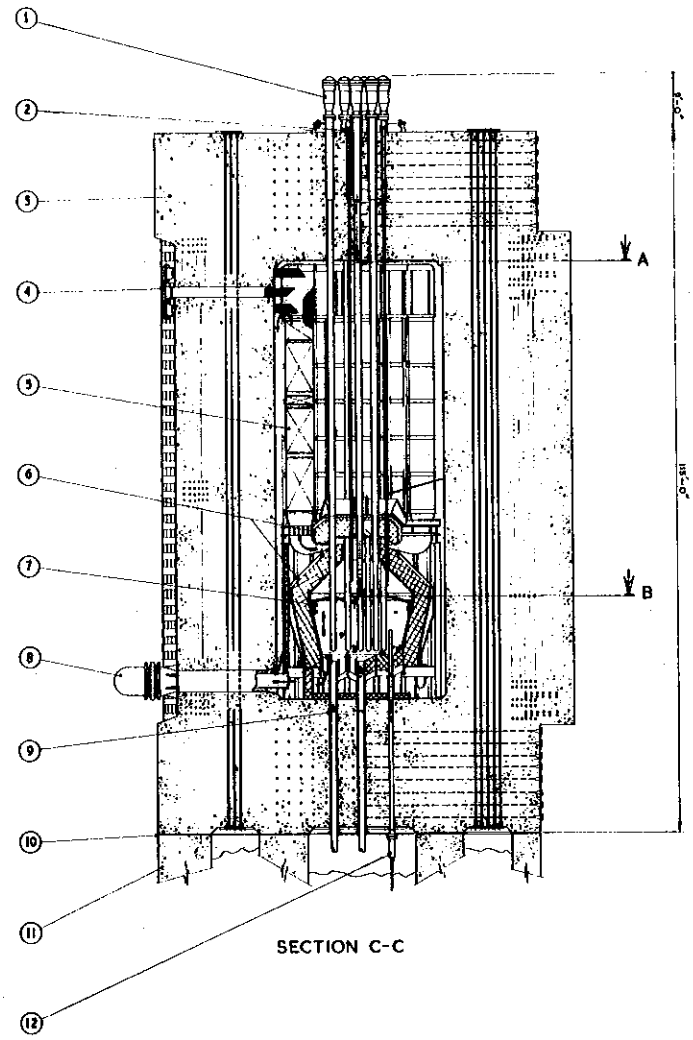
(h) Reactor Safety

(i) Loss of Forced Coolant Circulation

Coolant Temperature and Pressure	100% Rated Value
Natural Circulation Flow (%Rated Flow)	15%
Maximum Fuel Surface Temperature	1131 °C

(ii) Loss of Coolant Pressure

Maximum Allowable Fuel Temperature	1400 °C
Fuel Temperature after 60 minutes with Zero Coolant Flow	1400 °C
Maximum Fuel Temperature with 4 Circulators Operating	1370 °C
Maximum Fuel Temperature with 3 Circulators Operating and with Emergency Unload of Fuel	1340 °C
Coolant Flow per Circulator at Atmospheric Pressure and 3000 r.p.m.	3.0 lb/sec

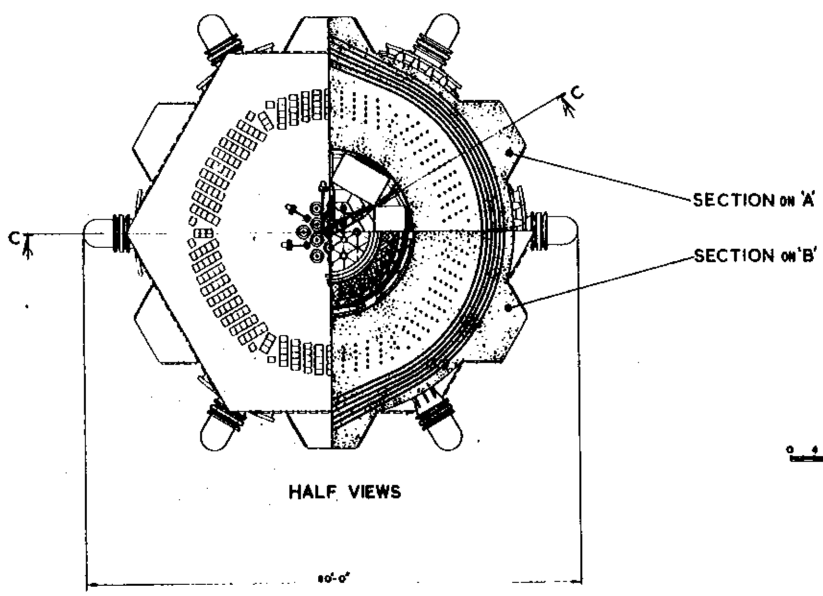


DESIGN DATA:

PRESTRESSED CONCRETE PRESSURE VESSEL:
28 ft INSIDE DIAMETER
72 ft INSIDE HEIGHT
16.5 ft WALL THICKNESS
21 ft FLAT END SLAB DEPTH

SYSTEM PRESSURE 1000 psig
CORE INLET GAS TEMP. 322° C
CORE OUTLET GAS TEMP. 865° C
BOILER INLET GAS TEMP. 750° C
AVERAGE CORE POWER DENSITY 11 W/cc
FUEL BALL DIAMETER 1.375 in
CORE TAPER 13¼°

1. SHUT DOWN ROD AND DRIVE MECHANISM SEE DRAWING BERS 325
2. FUEL BALL INLET MECHANISM SEE DRAWING BERS 327
3. PRESTRESSED CONCRETE PRESSURE VESSEL (P.C.P.V.) SEE DRAWING BERS 333
4. BOILER TUBE PLATE
5. BOILERS – 6 'ONCE THROUGH' UNITS SEE DRAWING BERS 332
6. REFLECTOR AND BY-PASS COOLING SEE DRAWING CERS 337
7. PEBBLE BED REACTOR CORE AND STRUCTURE SEE DRAWING BERS 329
8. AXIAL FLOW GAS CIRCULATOR SEE DRAWING BERS 324
9. FUEL BALL EXTRACT DEVICE SEE DRAWING CERS 342
10. RESILIENT VESSEL SUPPORT PAD
11. VESSEL SUPPORTS
12. SHIM ROD AND DRIVE MECHANISM SEE DRAWING CERS 326



SCALE IN FEET

FIGURE 1. SECTION OF UPFLOW H.T.G.C. PEBBLE BED REACTOR — REACTOR ARRANGEMENT

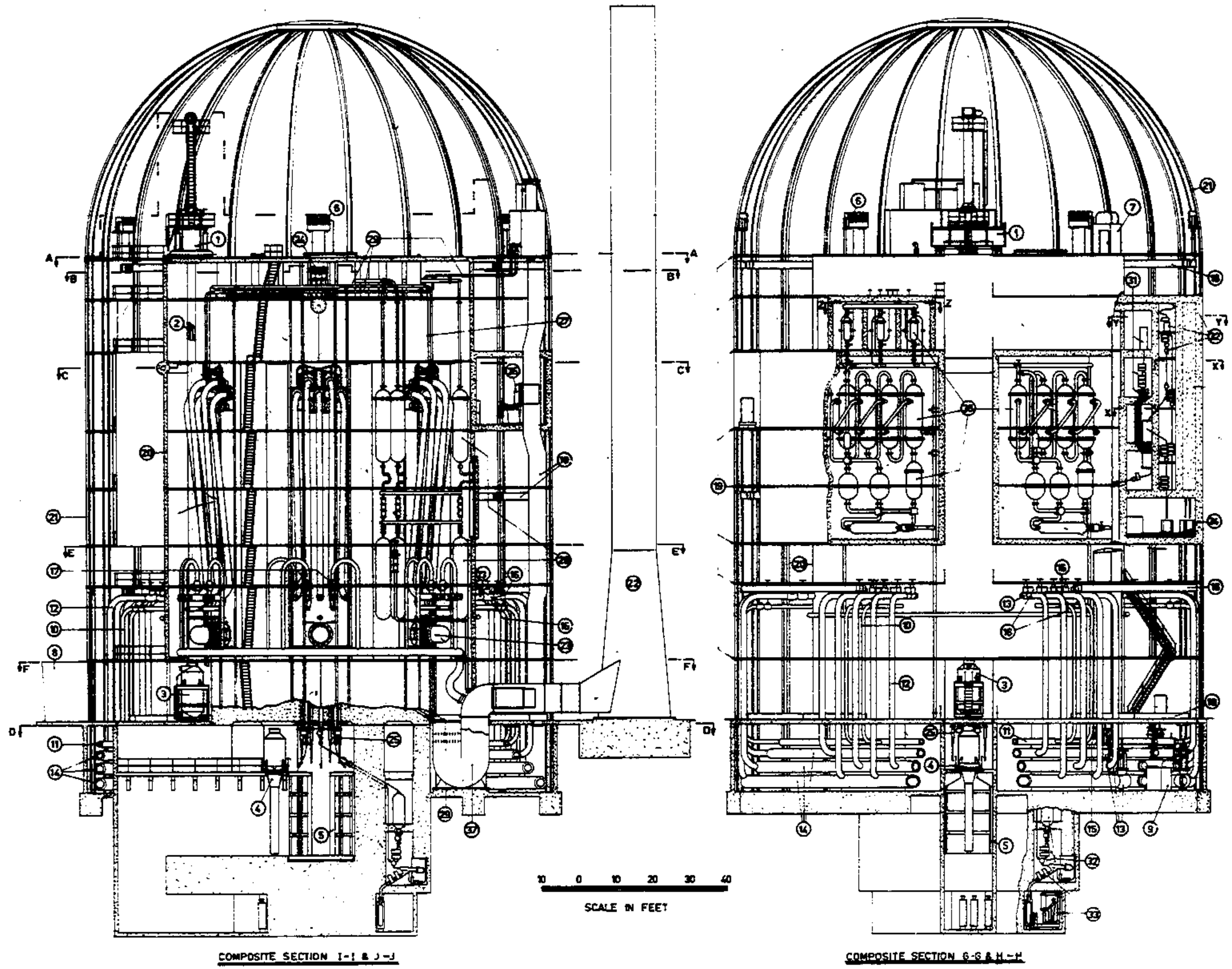


FIGURE 2(a) GENERAL ARRANGEMENT OF REACTOR PLANT — ELEVATION

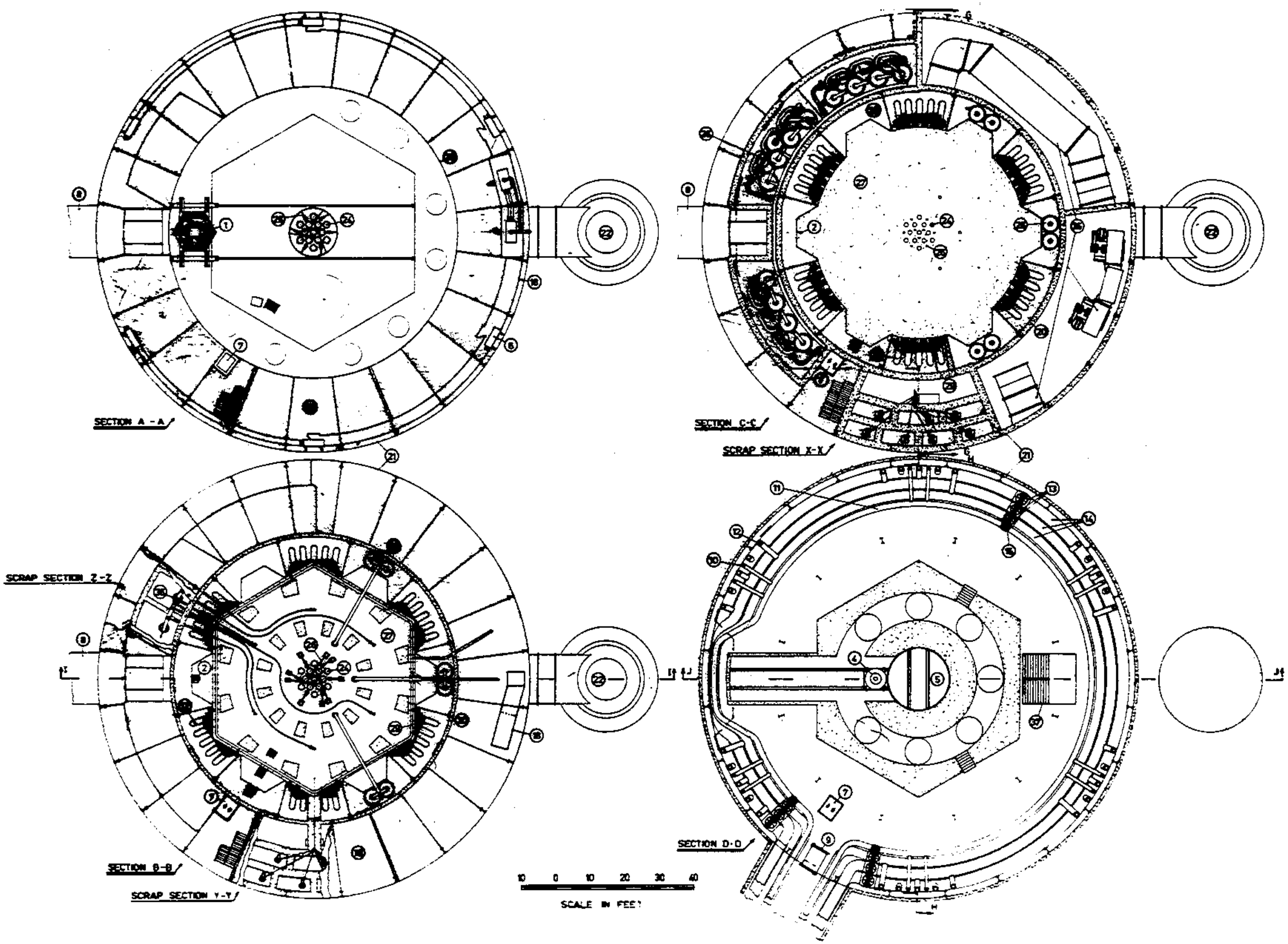
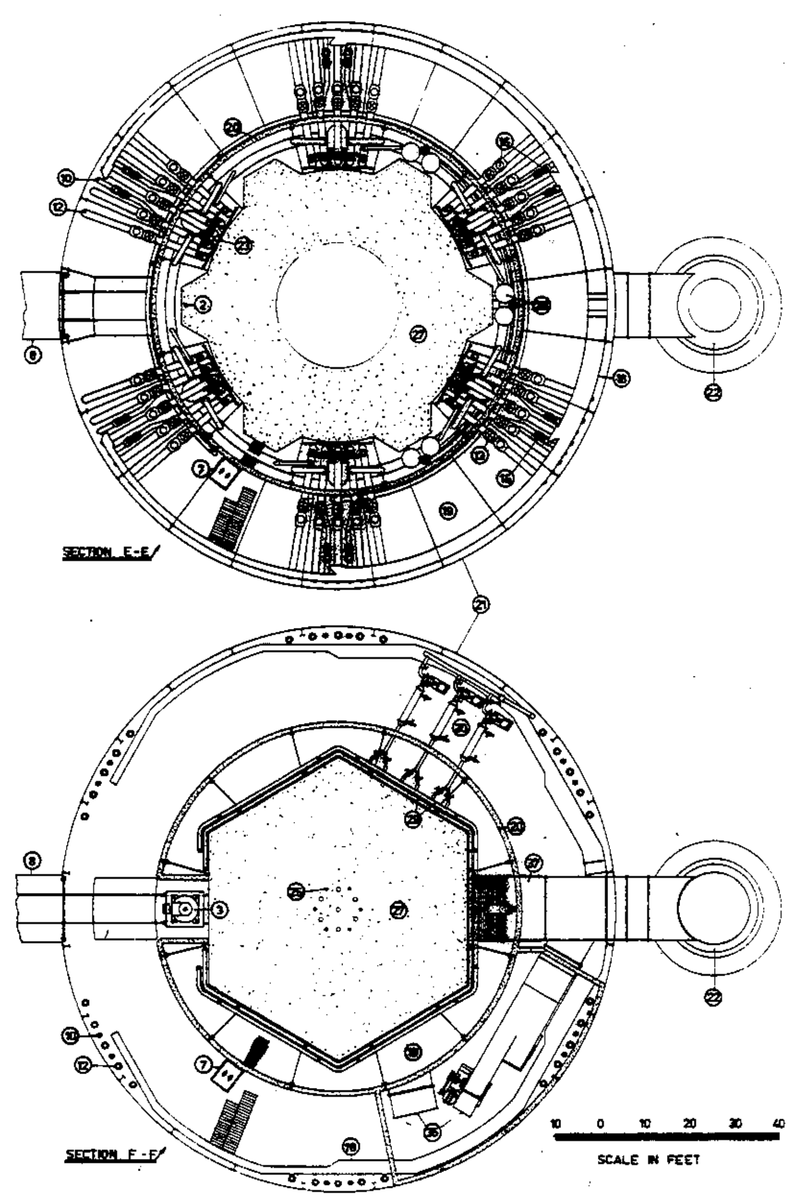


FIGURE 2(b) GENERAL ARRANGEMENT OF REACTOR PLANT — PLAN SECTIONS A, B, C, D



LEGEND

1. FLASK FOR REMOVAL OF SHUTDOWN RODS & GUIDE TUBES
2. SHIELDED TRANSFER FACILITY FOR SHUTDOWN RODS
3. TRANSPORT FLASK FOR SHUTDOWN RODS & CONTROL RODS
4. FLASK FOR REMOVAL OF CONTROL RODS
5. FLASK TURNTABLE
6. AIR CONDITIONING UNIT - 6 OFF
7. PERSONNEL LIFT
8. VEHICLE AIR LOCK
9. EMERGENCY AIR LOCK
10. FEED WATER RISERS
11. FEED WATER HEADER
12. STEAM DOWNCOMERS
13. STEAM CIRCUIT ISOLATING VALVES
14. STEAM HEADERS
15. FEED WATER ISOLATING VALVES
16. NON-RETURN VALVES
17. SAFETY VALVES - 18 OFF
18. CONTAINMENT BUILDING VENTILATION DUCTWORK
19. EXPANDED METAL FLOORING
20. INNER CONCRETE CONTAINMENT STRUCTURE
21. OUTER WELDED STEEL CONTAINMENT BUILDING
22. STACK - 250 FT. HIGH
23. AXIAL FLOW GAS CIRCULATOR - 6 OFF
24. SHUT DOWN ROD & MECHANISM - 13 OFF
25. MAGNETIC JACK TYPE CONTROL ROD - 6 OFF
26. FUEL BALL INLET MECHANISM - 9 OFF
27. PRESTRESSED CONCRETE PRESSURE VESSEL
28. REACTOR COOLANT BLOWDOWN SYSTEM
29. REACTOR PRESSURE VESSEL COOLING PIPEWORK
30. REACTOR PRESSURE VESSEL COOLING PLANT
31. FRESH FUEL INLET HANDLING FACILITY
32. IRRADIATED FUEL HANDLING FACILITY
33. DAMAGED FUEL HANDLING FACILITY
34. SPENT FUEL HANDLING FACILITY
35. VENTILATION AND CLEANUP PLANT
36. REACTOR COOLANT (CO_2) PURIFICATION PLANT
37. VENTILATION AIR EXHAUST DUCT & WATER SEAL.

FIGURE 2(c) GENERAL ARRANGEMENT OF REACTOR PLANT — PLAN SECTIONS E, F

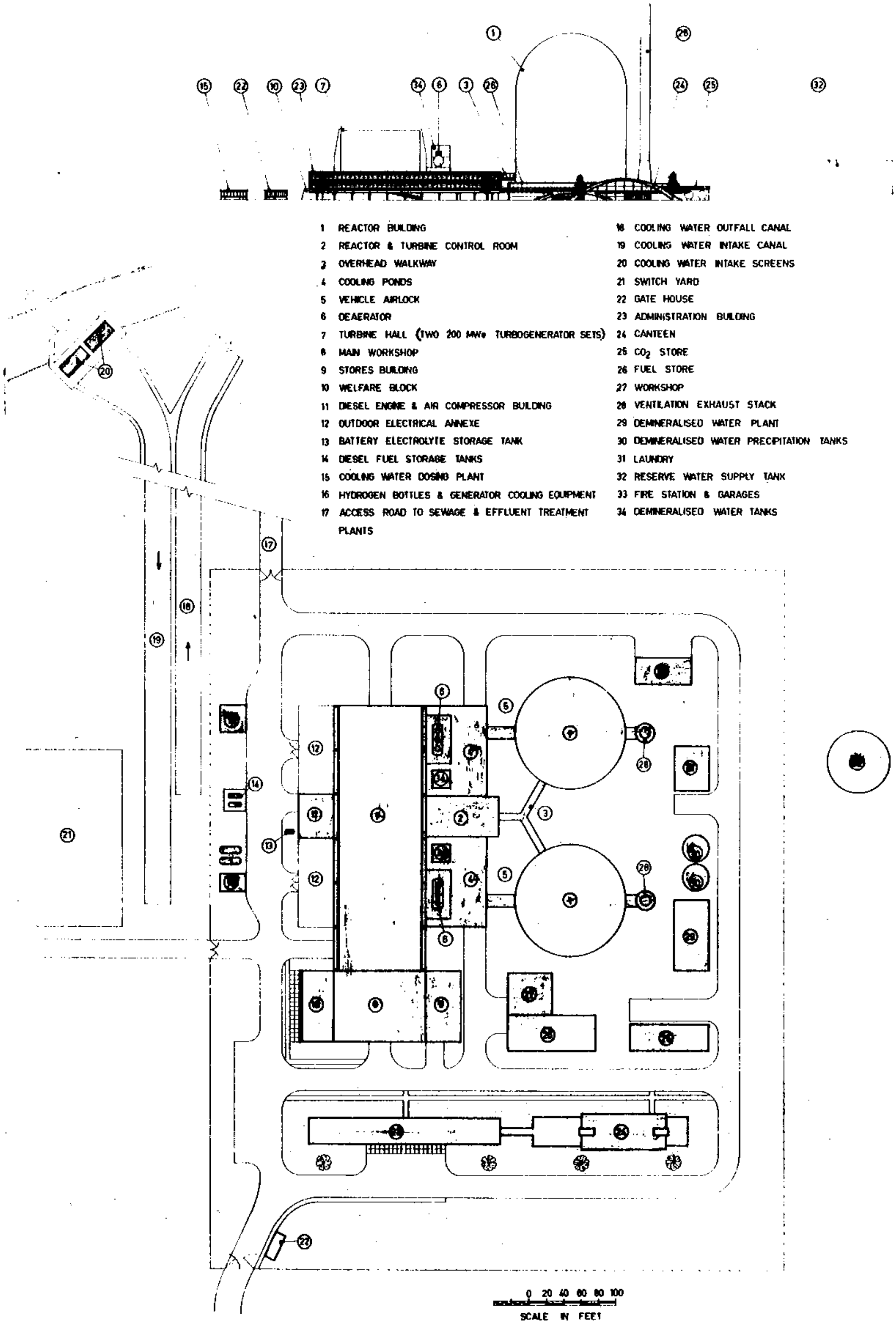


FIGURE 3. SITE PLAN AND ELEVATION OF TWIN P.B.R. REACTOR STATION

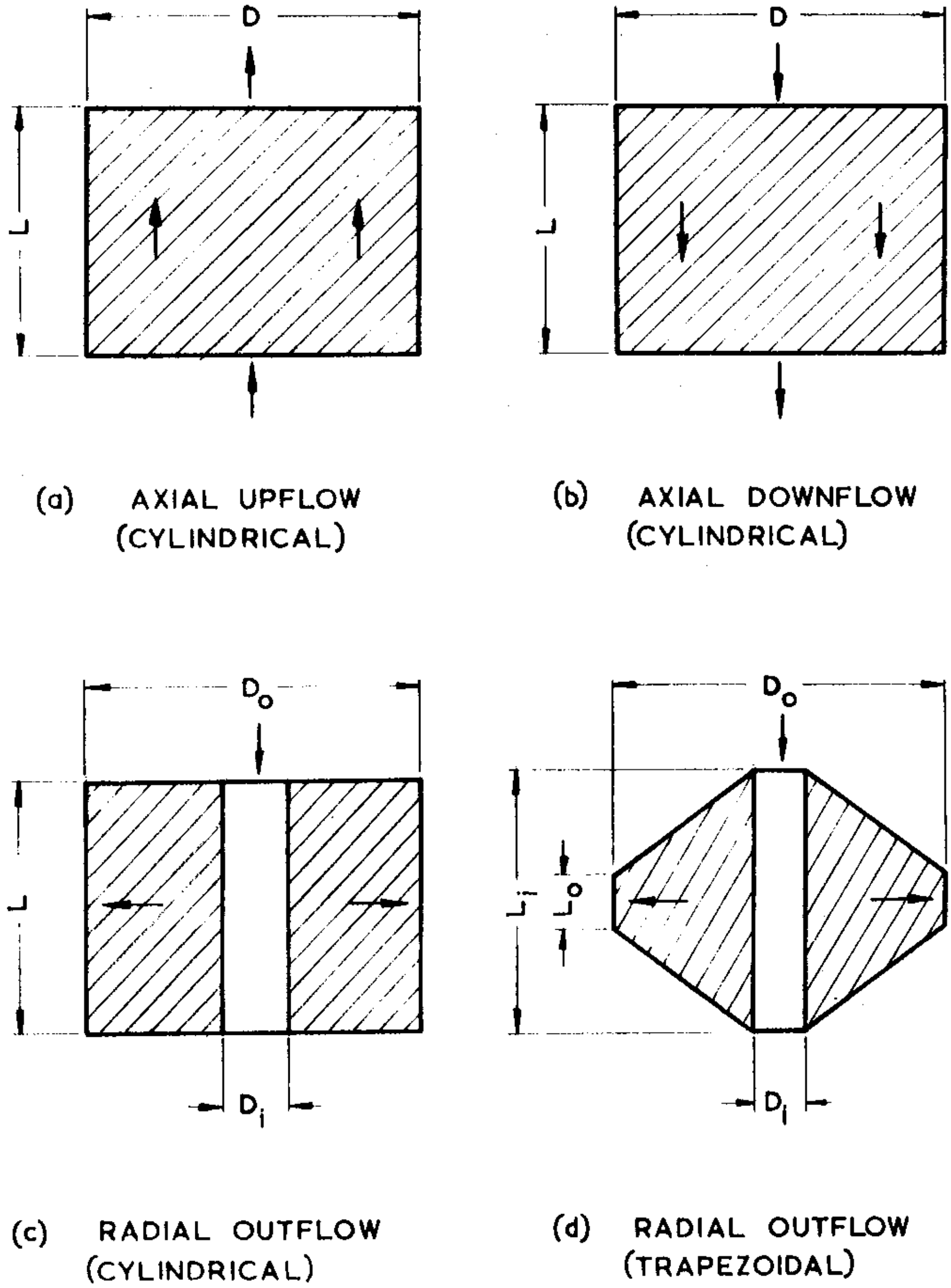


FIGURE 4. COOLANT FLOW ARRANGEMENTS FOR VARIOUS P.B.R. CORE TYPES

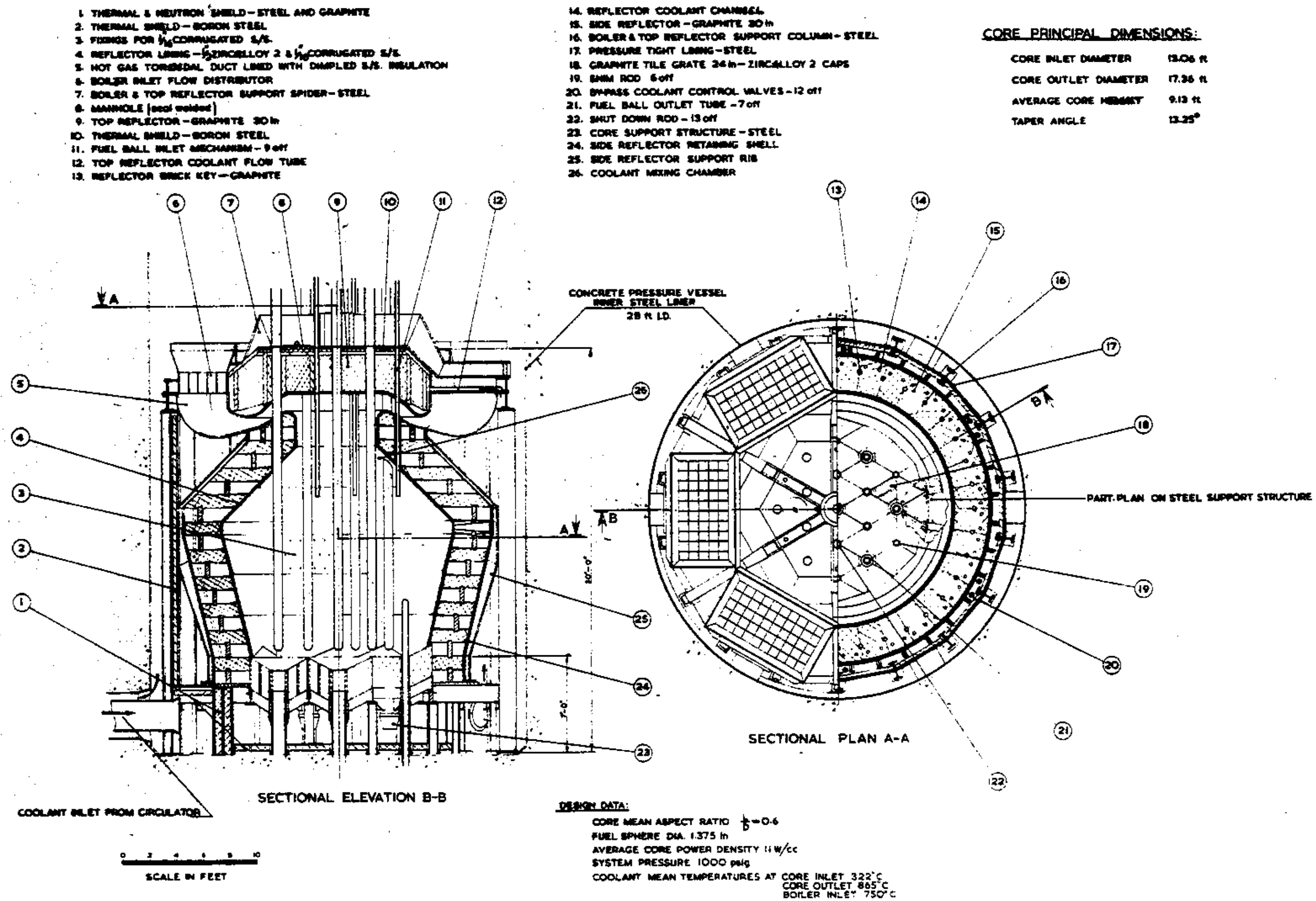


FIGURE 5(a) SECTION OF UPFLOW CORE SHOWING CONTROL ROD, BOILER ARRANGEMENT, AND SUPPORT STRUCTURE

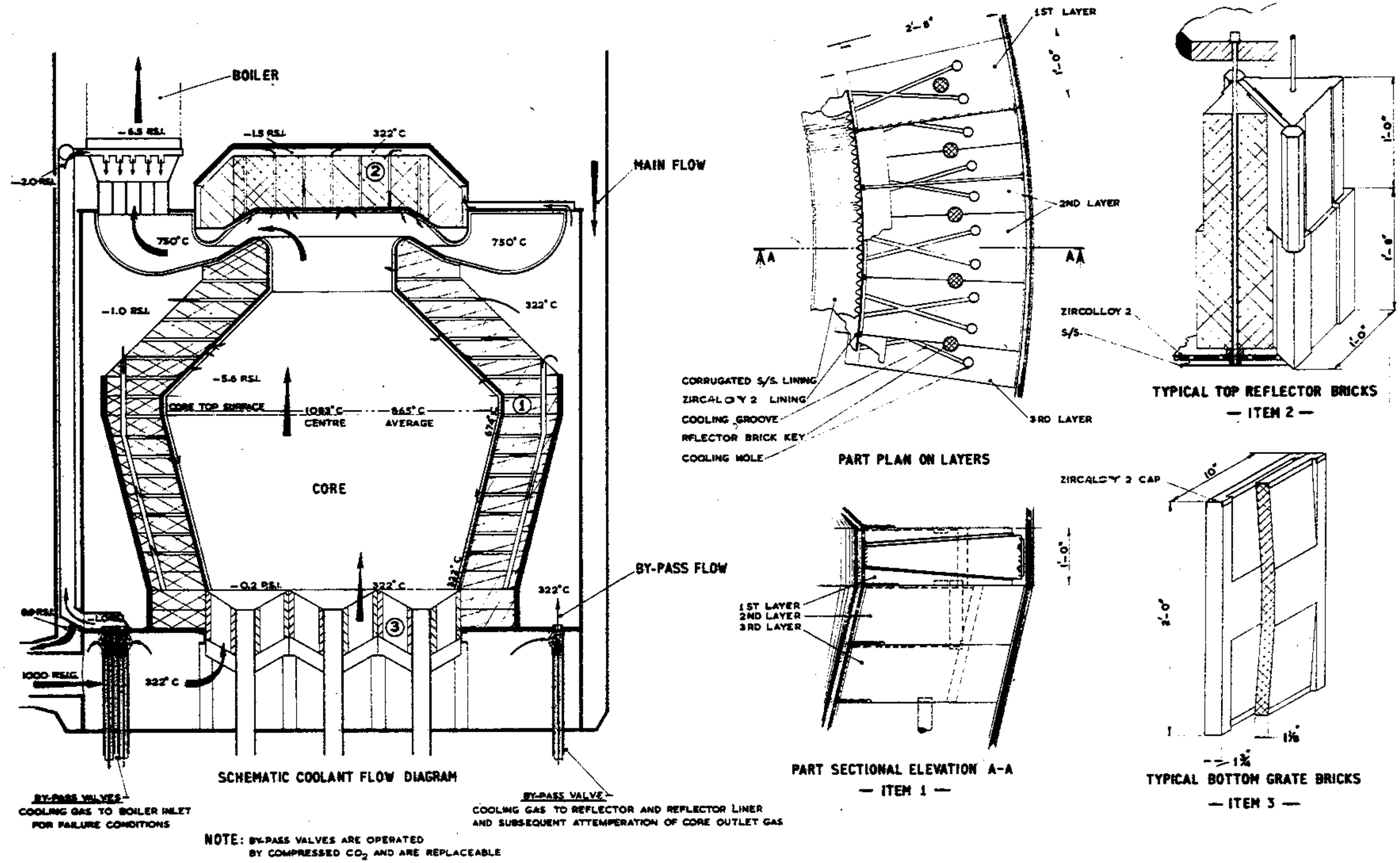


FIGURE 5(b) SECTION OF UPFLOW CORE SHOWING REFLECTOR ARRANGEMENT

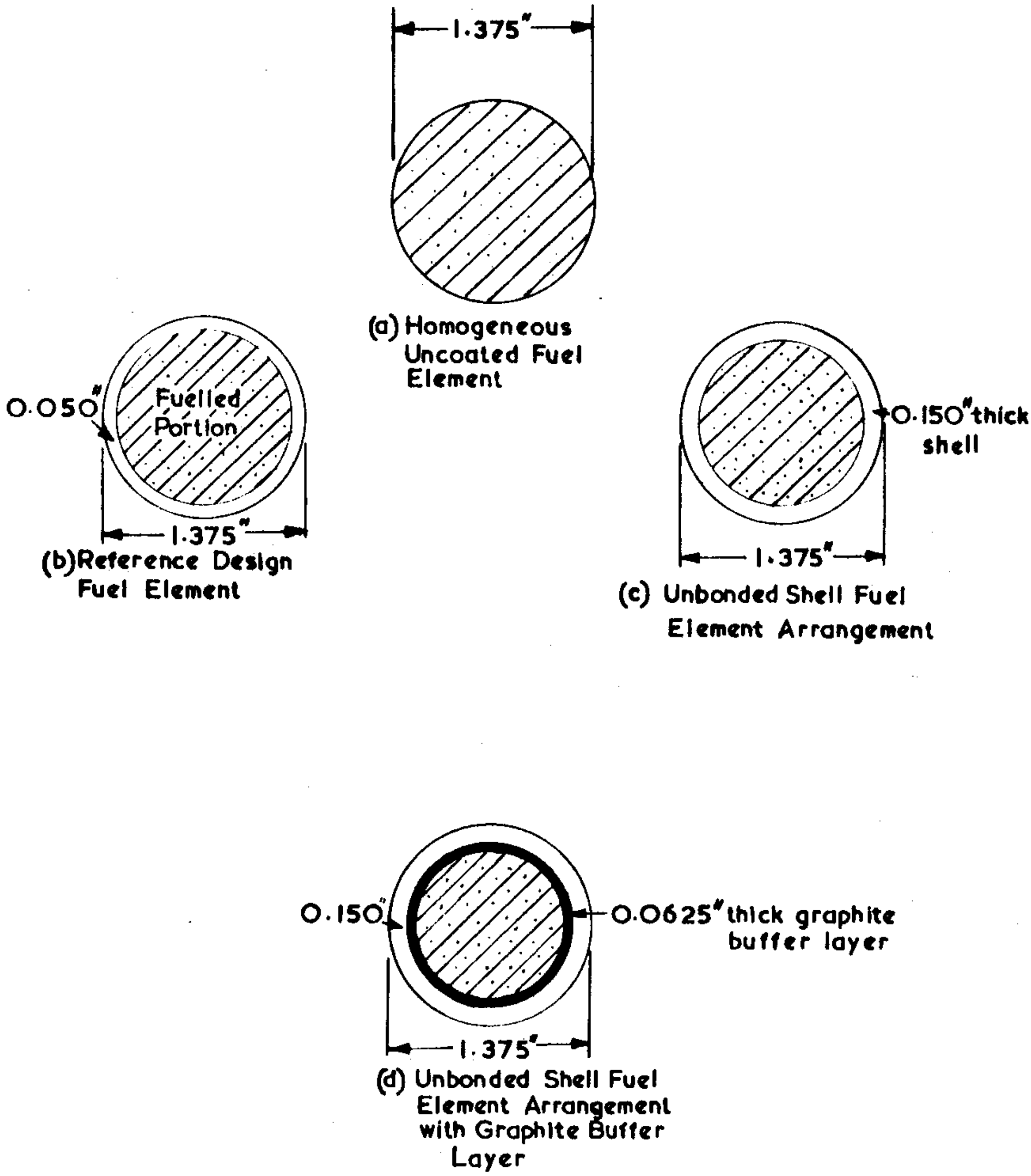


FIGURE 6. ALTERNATIVE TYPES OF SPHERICAL FUEL ELEMENT DESIGNS CONSIDERED

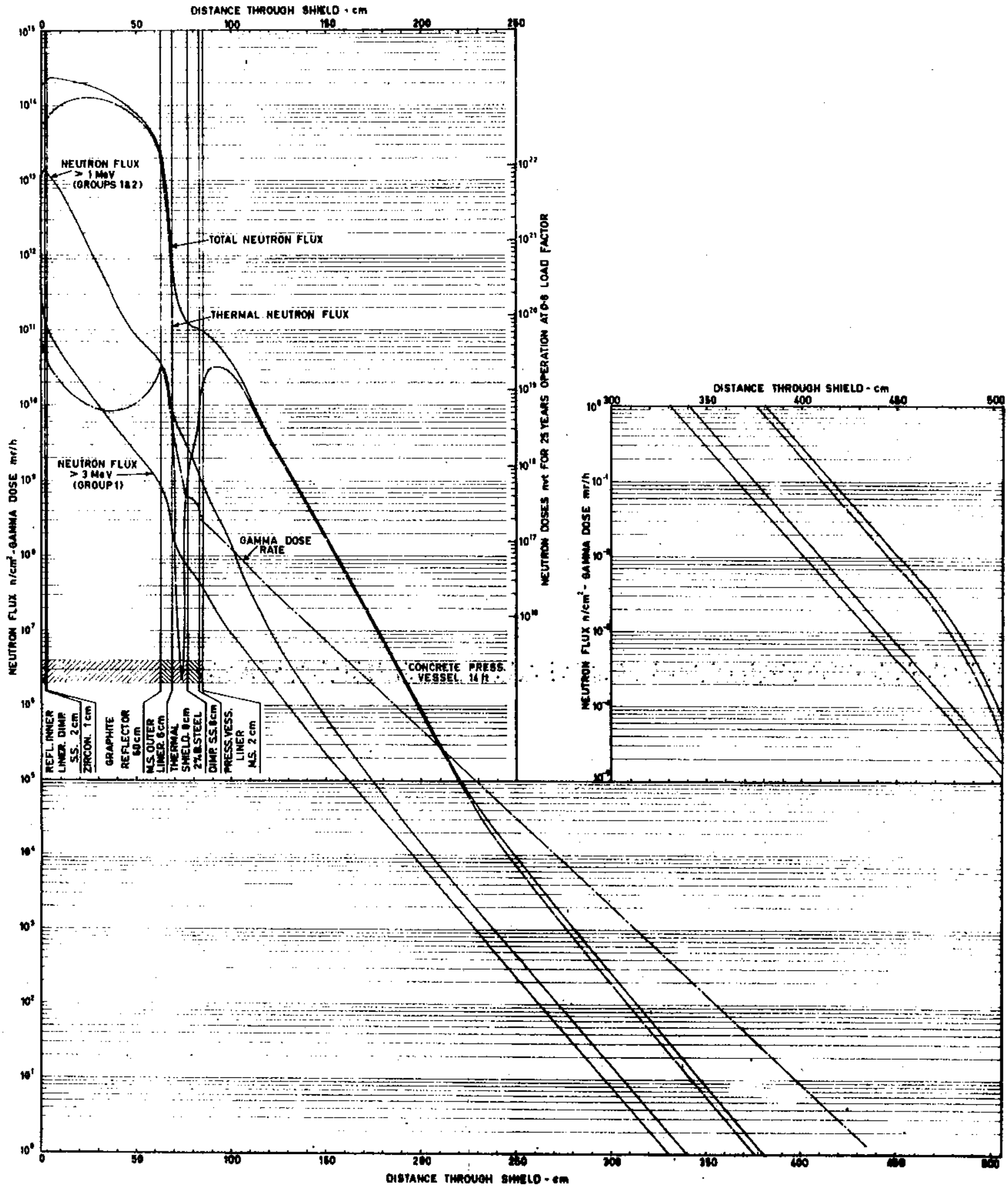


FIGURE 7. NEUTRON AND GAMMA FLUX LEVELS AND DOSES IN THE SHIELDING OF THE P.B.R.

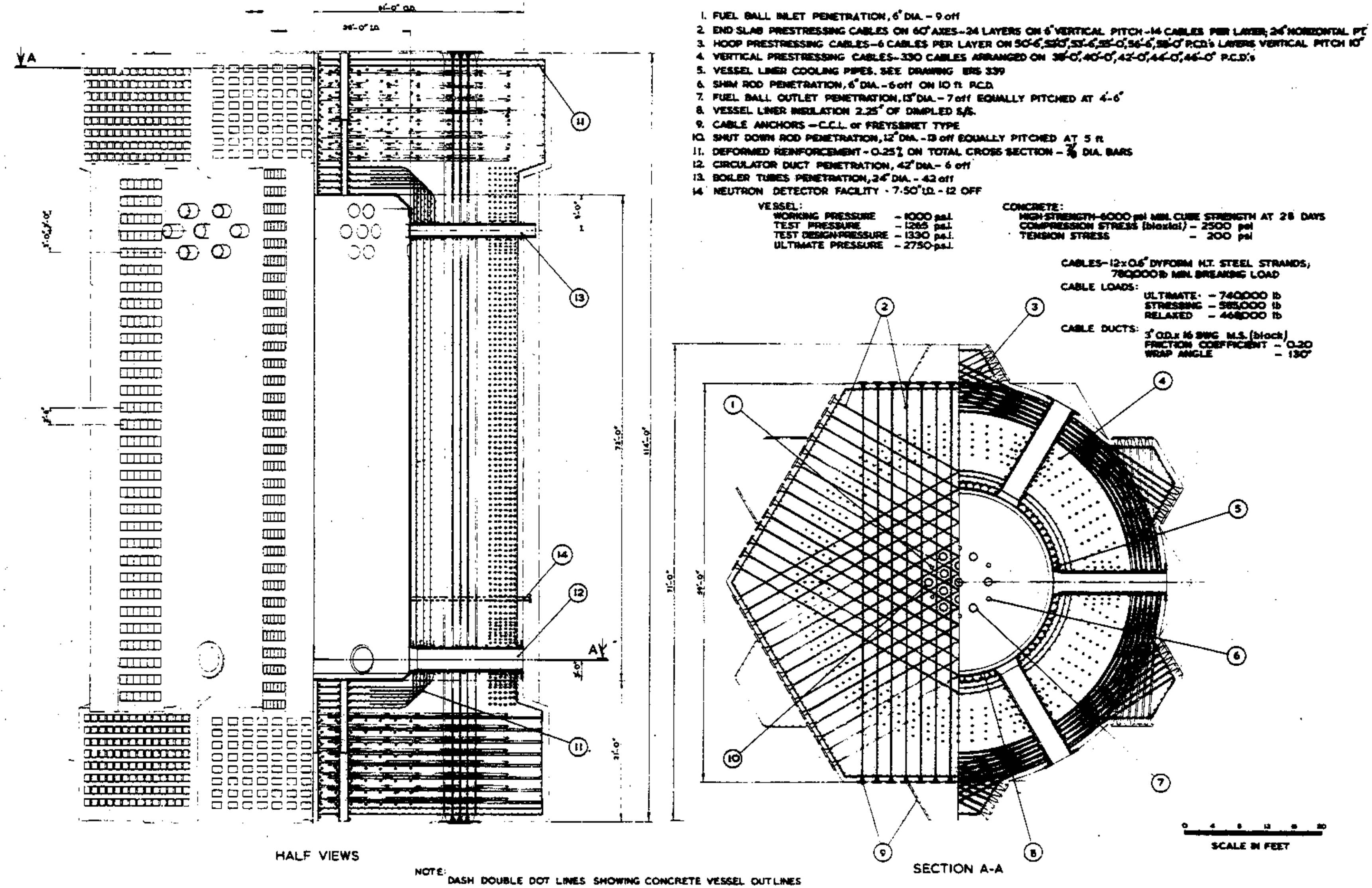
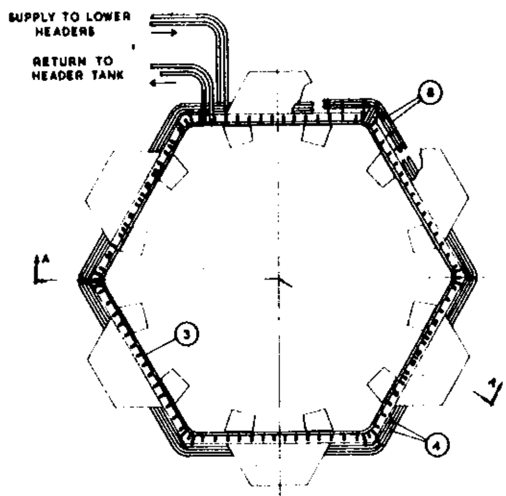
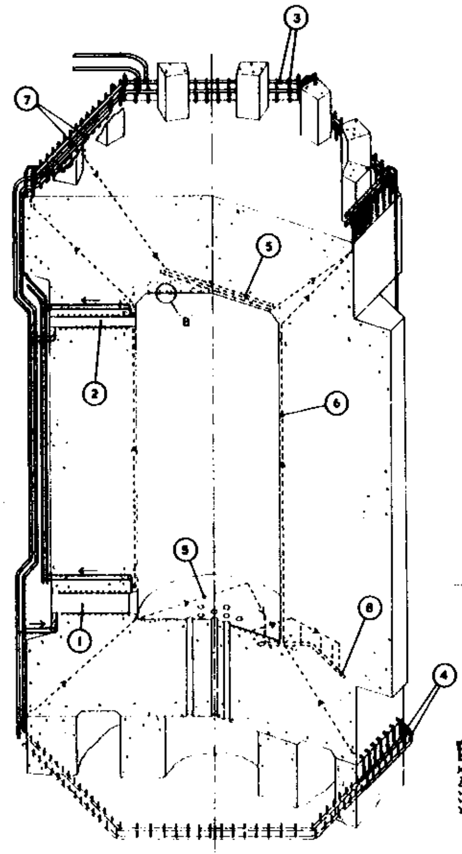


FIGURE 8. PRE-STRESSED CONCRETE PRESSURE VESSEL ARRANGEMENT AND CABLING DETAILS

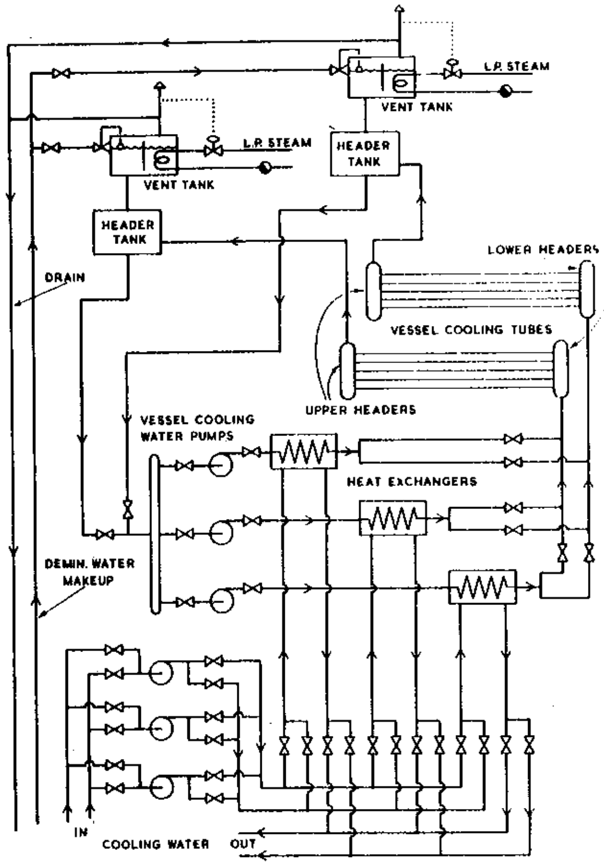




SECT. AA.

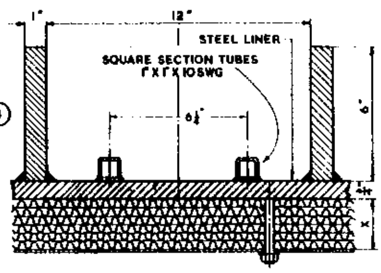
DIAGRAMMATIC ARRANGEMENT OF VESSEL COOLING TUBES

1. CIRCULATOR PENETRATION
2. STEAM PIPE PENETRATION
3. UPPER HEADERS - OUTLET
4. LOWER HEADERS - INLET
5. VESSEL END SLAB COOLING TUBES
6. VESSEL VERTICAL WALL COOLING TUBES
7. INLET HEADER - TOP SLAB COOLING
8. OUTLET HEADER - BOTTOM SLAB COOLING



VESSEL COOLING WATER SYSTEM — SCHEMATIC

LEGEND
LEVEL CONTROL VALVE
BOILING CONTROL VALVE
STEAM TRAP



INSULATION THICKNESS	X
CIRCULATORS	$1\frac{3}{4}$"
STEAM PENETRATIONS	$1\frac{3}{4}$"
MAIN VESSEL SURFACES :-	
VERTICAL	$2\frac{1}{4}$"
HORIZONTAL	$1\frac{3}{4}$"

DETAIL 'B'

SHOWING ARRANGEMENT OF STAINLESS STEEL FOIL INSULATION, MILD STEEL VESSEL LINER AND LINER ANCHOR RIBS, AND COOLING WATER PIPES

NOTE:

PRESSURE VESSEL INTEGRITY IS DEPENDENT ON THE CONCRETE COOLING. BECAUSE OF THIS, THE COOLING SYSTEM IS DUPLICATED AT EVERY POINT. ONE SYSTEM, OPERATING ON ITS OWN, IS SUFFICIENT TO HOLD THE CONCRETE TEMPERATURE TO A SAFE VALUE. (60°C OR PERHAPS 70°C. NORMALLY OPERATES AT ABOUT 50°C.)

PENETRATION LINERS COOLED AS (1) AND (2) BUT NOT SHOWN, ARE CONTROL RODS, BALL FEED MECHANISMS, SHIM RODS, CO_2 VENTS AND SINGLERS.

FIGURE 9. PRE-STRESSED CONCRETE PRESSURE VESSEL COOLING SYSTEM

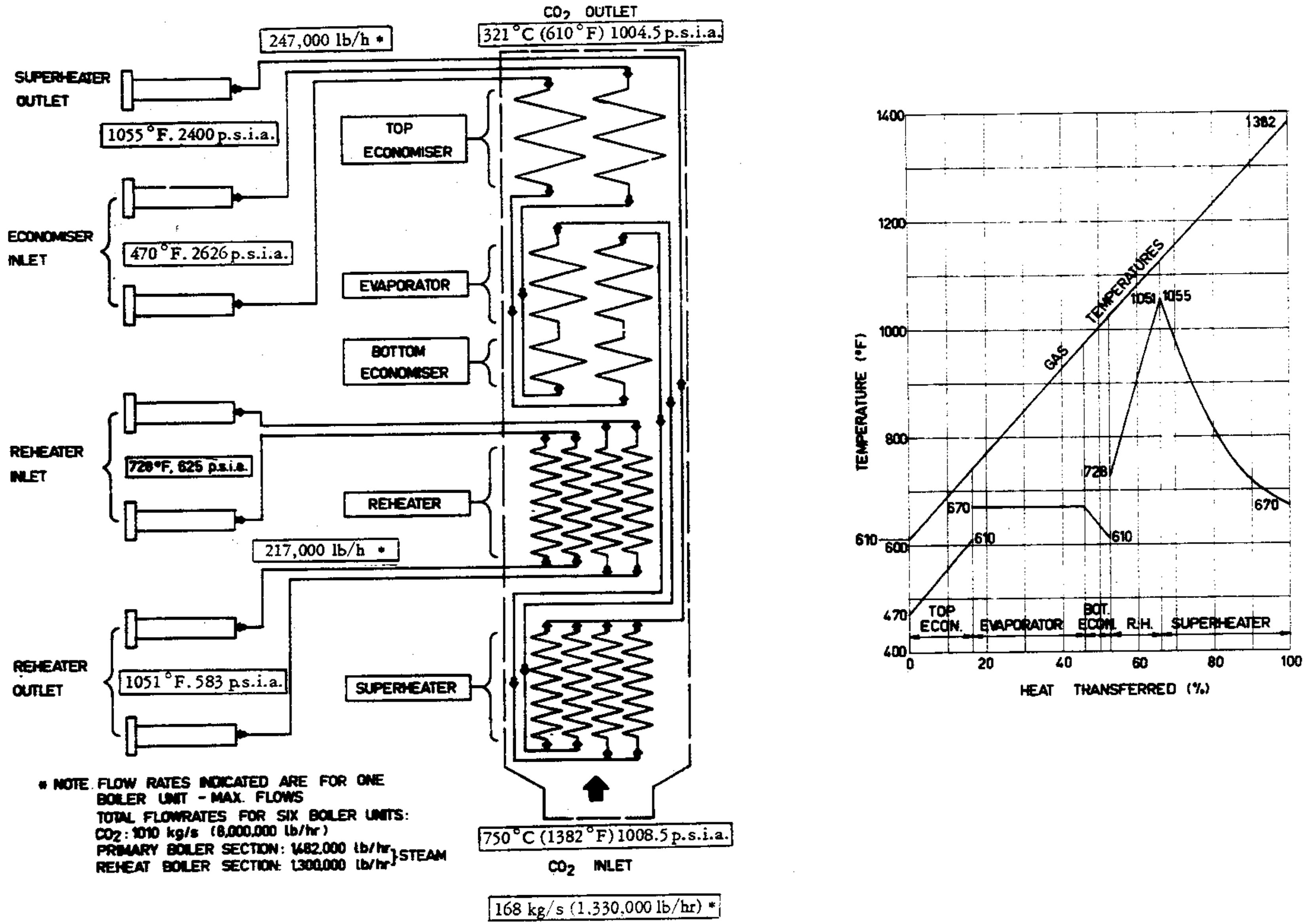


FIGURE 10. BOILER CIRCUIT AND TEMPERATURE ENTHALPY DIAGRAM FOR A 200 MWe UPFLOW P.B.R.

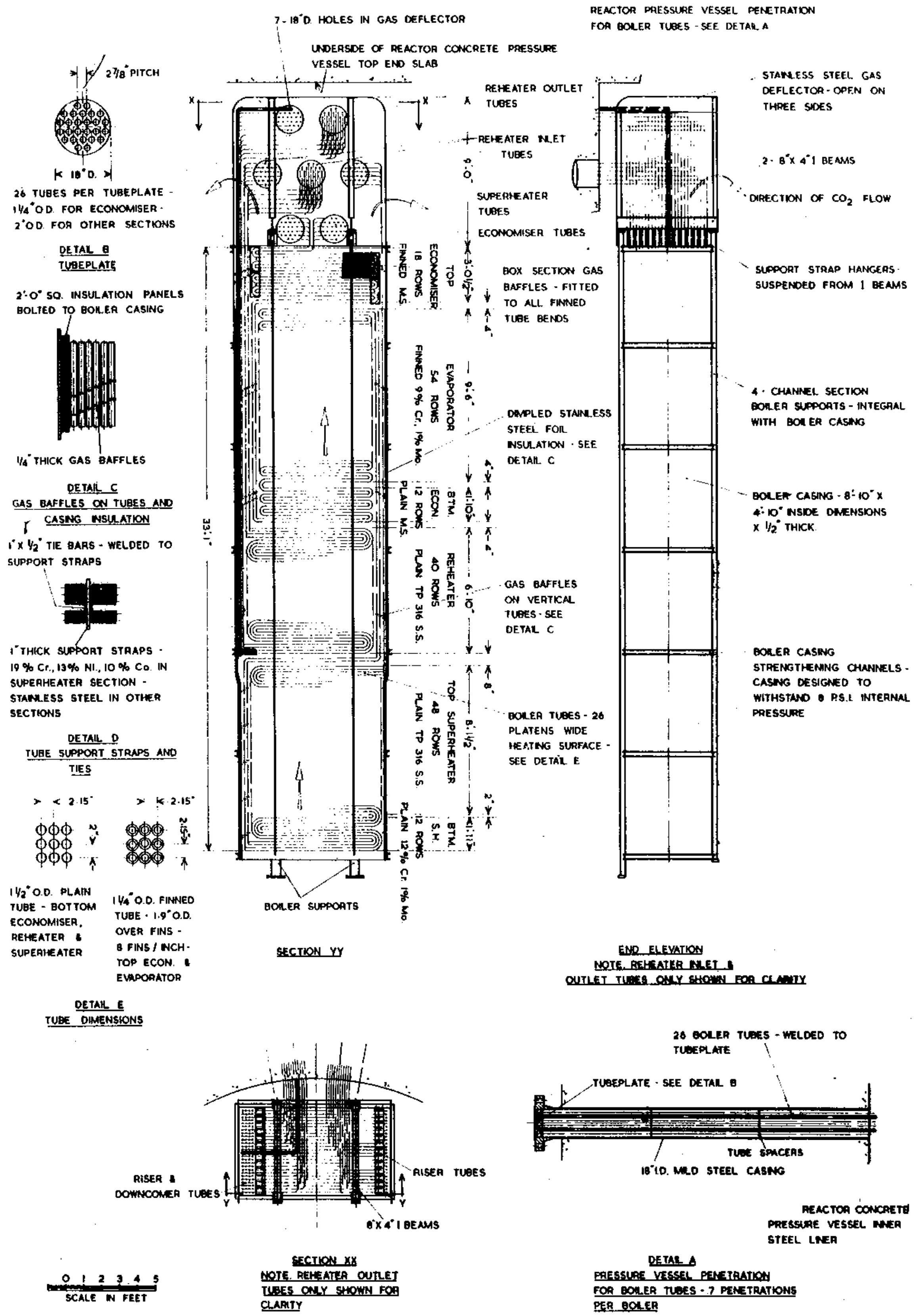


FIGURE 11. BOILER ARRANGEMENT FOR AN UPFLOW P.B.R.

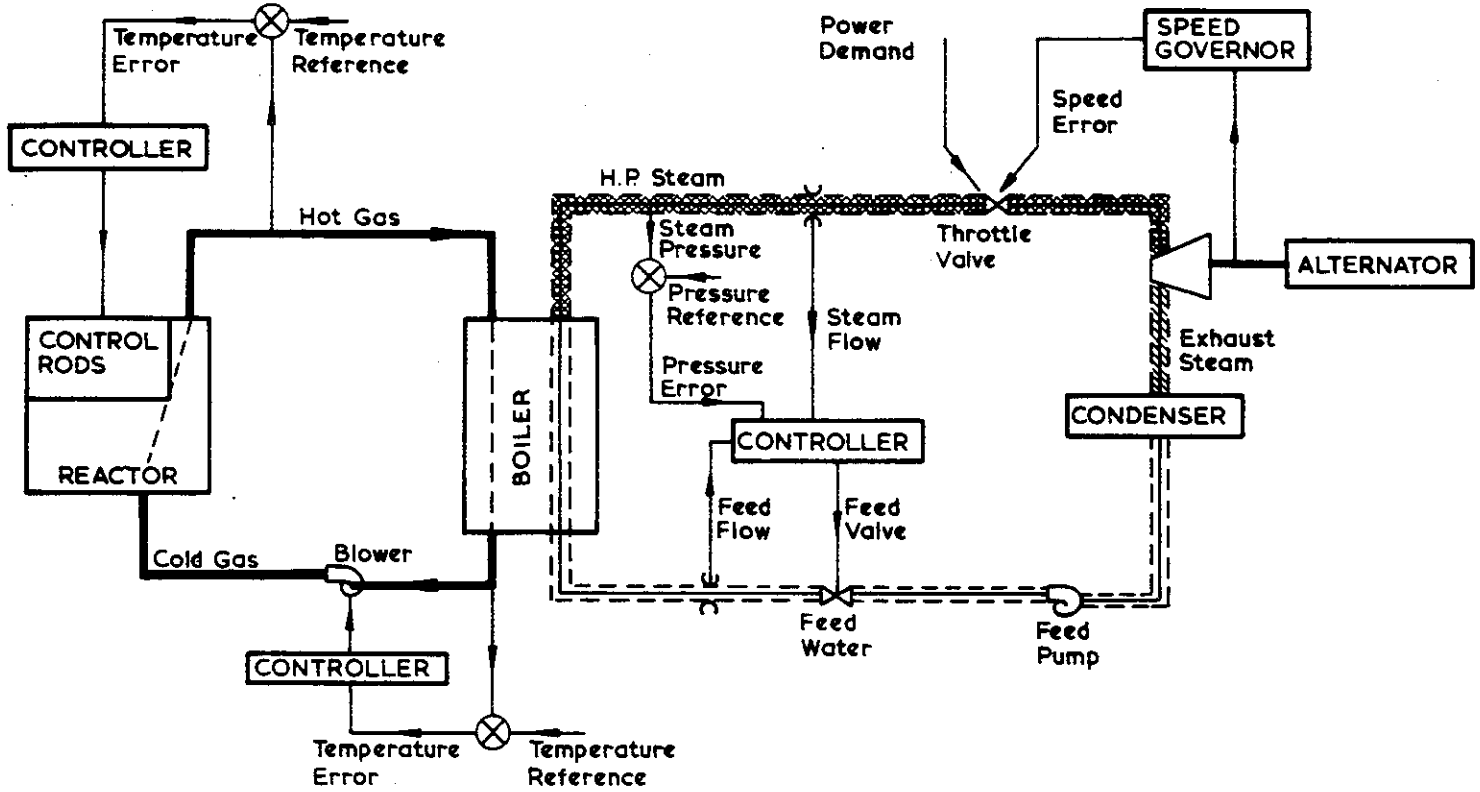


FIGURE 12. OVERALL CONTROL SCHEME FOR A P.B.R. — TURBO—GENERATOR SYSTEM

P 388

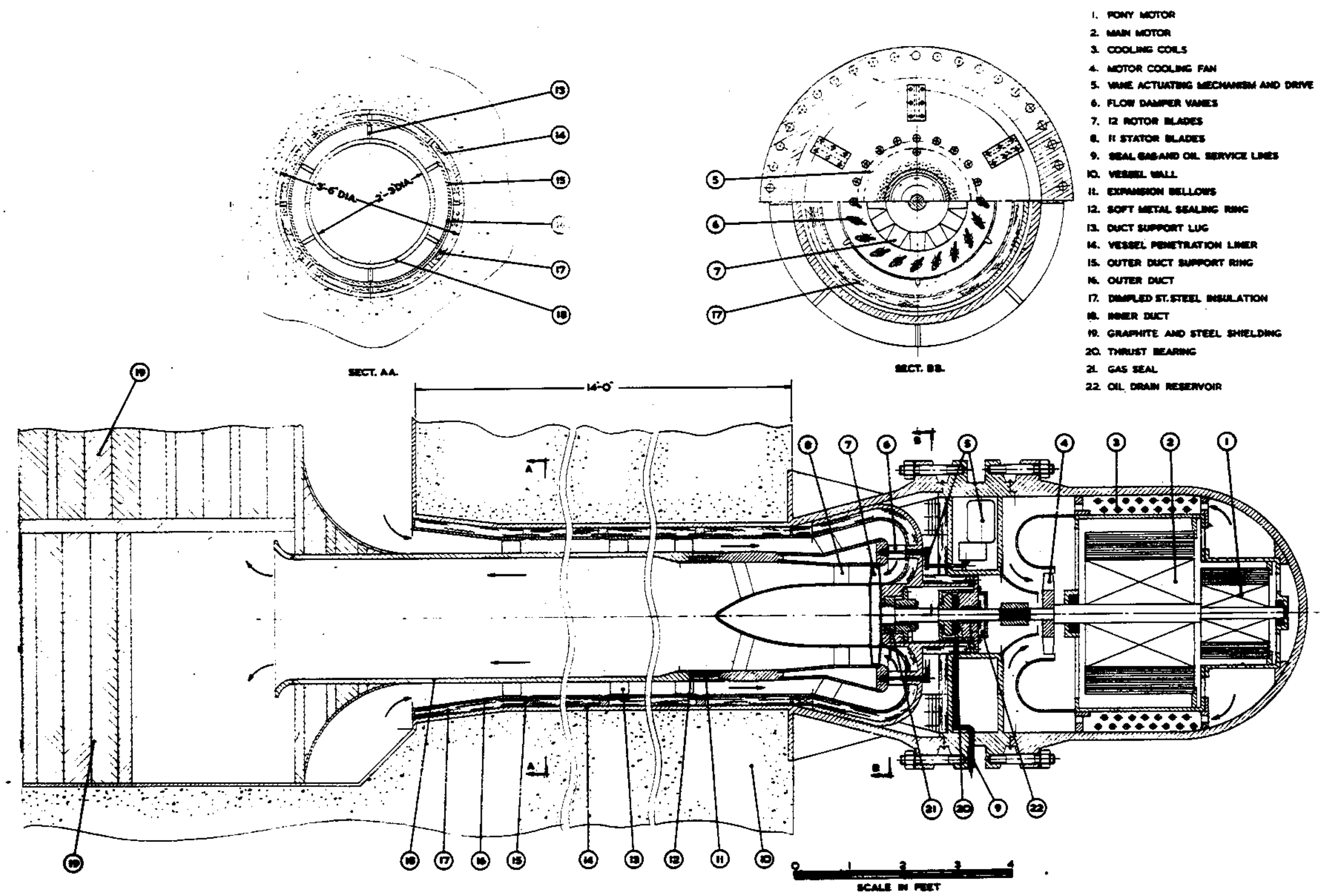


FIGURE 13. GAS CIRCULATOR ASSEMBLY FOR THE UPFLOW P.B.R REFERENCE DESIGN

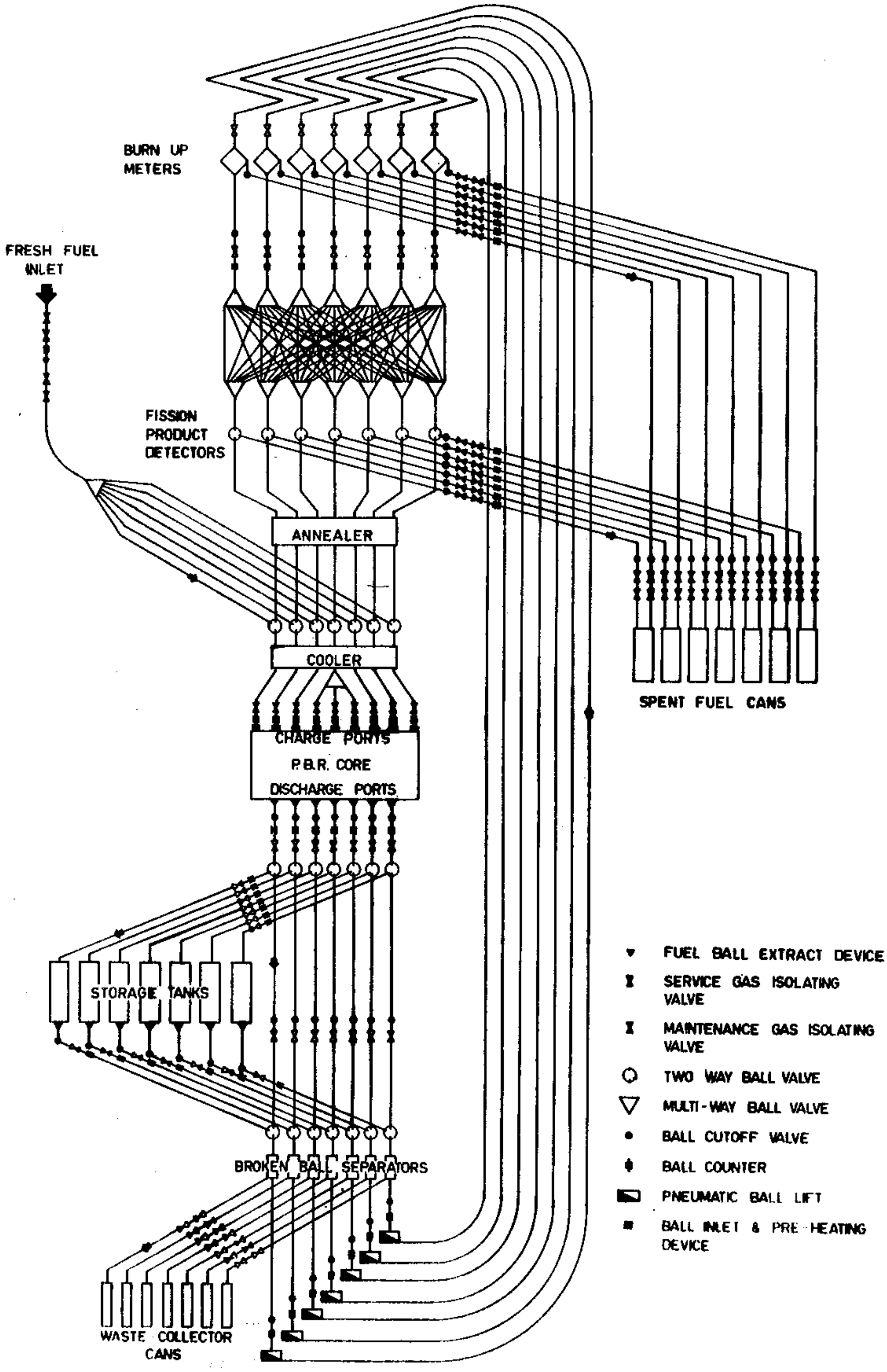


FIGURE 14. FUEL HANDLING FLOWCHART FOR A P.B.R.

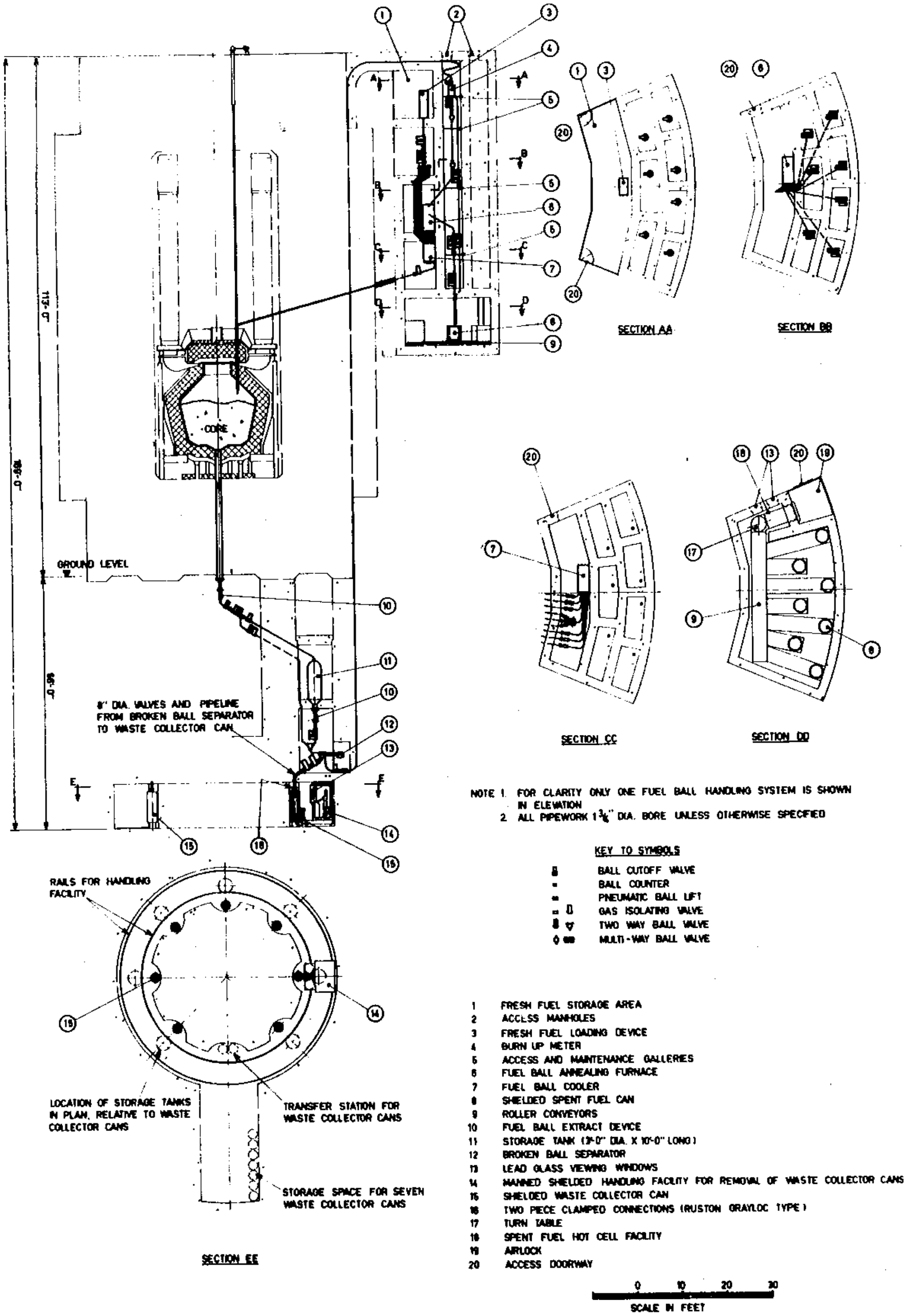
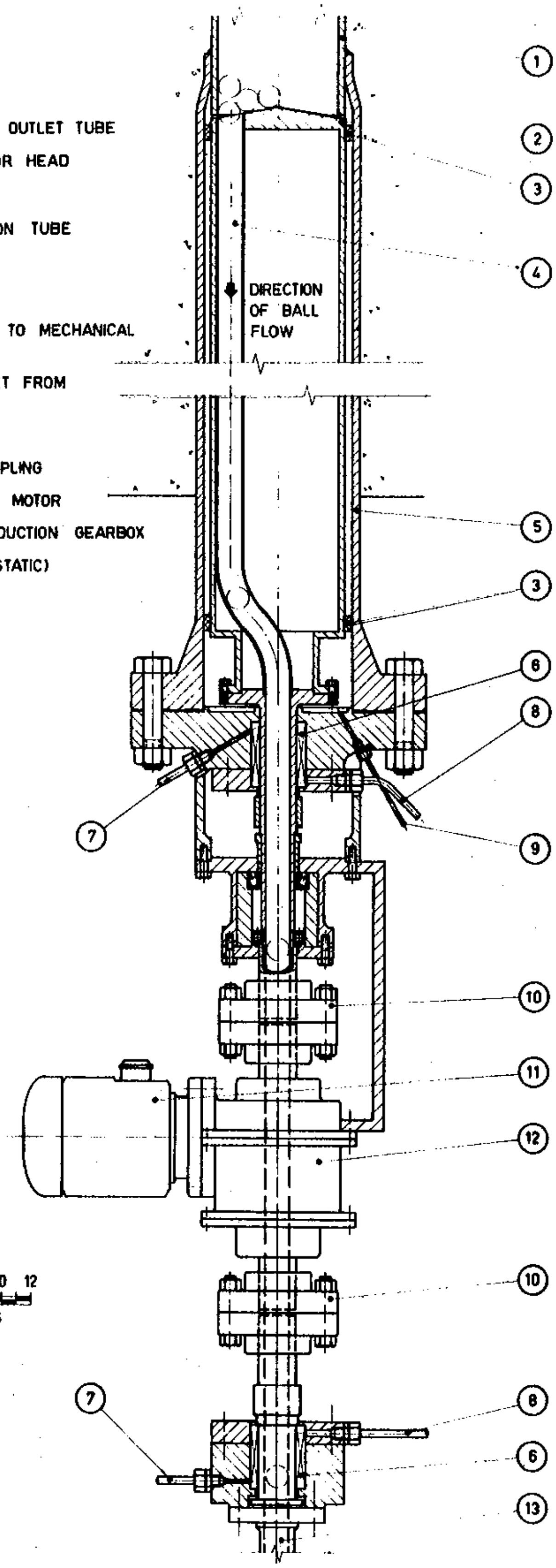


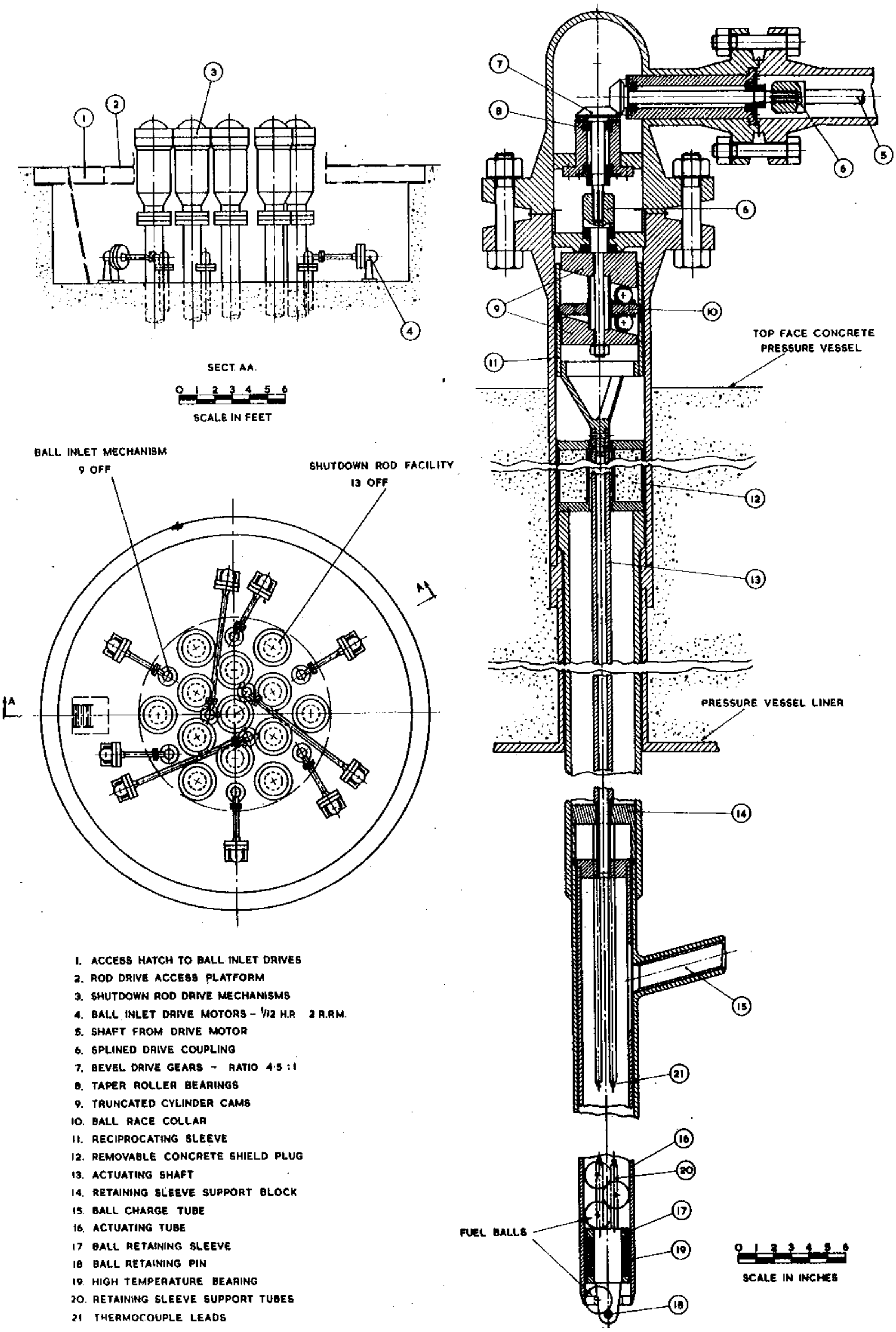
FIGURE 15. LAYOUT OF HANDLING SYSTEM FOR A P.B.R.

1 REACTOR FUEL BALL OUTLET TUBE
2 FUEL BALL EXTRACTOR HEAD
3 GRAPHITE RING
4 FUEL BALL EXTRACTION TUBE
5 SHROUD TUBE
6 MECHANICAL SEAL
7 SEALING FLUID INLET TO MECHANICAL SEAL
8 SEALING FLUID OUTLET FROM MECHANICAL SEAL
9 GAS BLEED INLET
10 PRESSURE TIGHT COUPLING
11 TWO SPEED ELECTRIC MOTOR
12 WORM & WHEEL REDUCTION GEARBOX
13 BALL OUTLET TUBE (STATIC)



0 2 4 6 8 10 12
SCALE IN INCHES

FIGURE 16. FUEL ELEMENT EXTRACT DEVICE FOR A P.B.R.



1. ACCESS HATCH TO BALL INLET DRIVES
2. ROD DRIVE ACCESS PLATFORM
3. SHUTDOWN ROD DRIVE MECHANISMS
4. BALL INLET DRIVE MOTORS – 1/12 H.P. 2 R.P.M.
5. SHAFT FROM DRIVE MOTOR
6. SPLINED DRIVE COUPLING
7. BEVEL DRIVE GEARS ~ RATIO 4·5 : 1
8. TAPER ROLLER BEARINGS
9. TRUNCATED CYLINDER CAMS
10. BALL RACE COLLAR
11. RECIPROCATING SLEEVE
12. REMOVABLE CONCRETE SHIELD PLUG
13. ACTUATING SHAFT
14. RETAINING SLEEVE SUPPORT BLOCK
15. BALL CHARGE TUBE
16. ACTUATING TUBE
17. BALL RETAINING SLEEVE
18. BALL RETAINING PIN
19. HIGH TEMPERATURE BEARING
20. RETAINING SLEEVE SUPPORT TUBES
21. THERMOCOUPLE LEADS

FIGURE 17. FUEL ELEMENT INLET AND PREHEATING MECHANISM FOR A P.B.R.

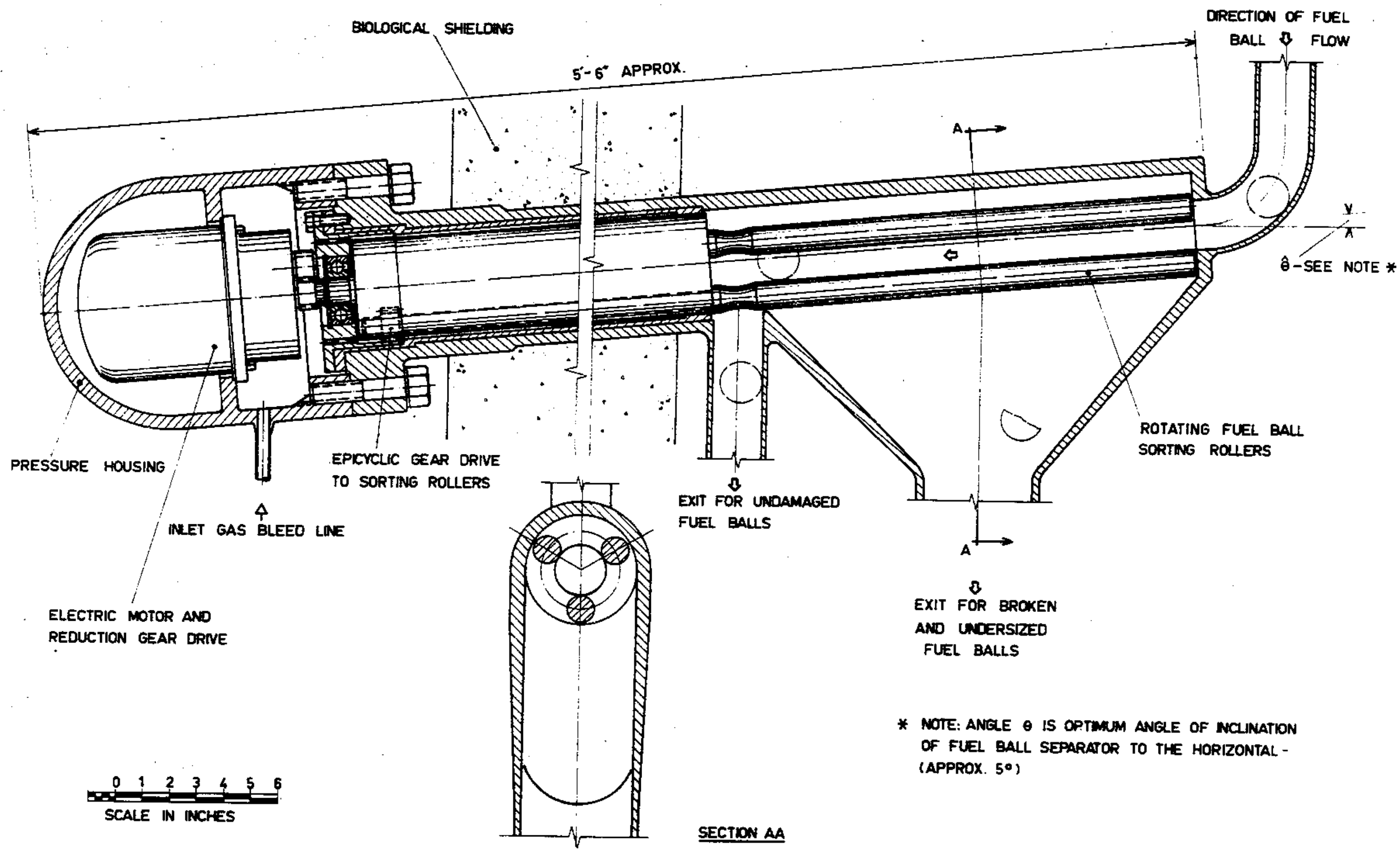


FIGURE 18. DAMAGED FUEL ELEMENT SEPARATOR FOR P.B.R.

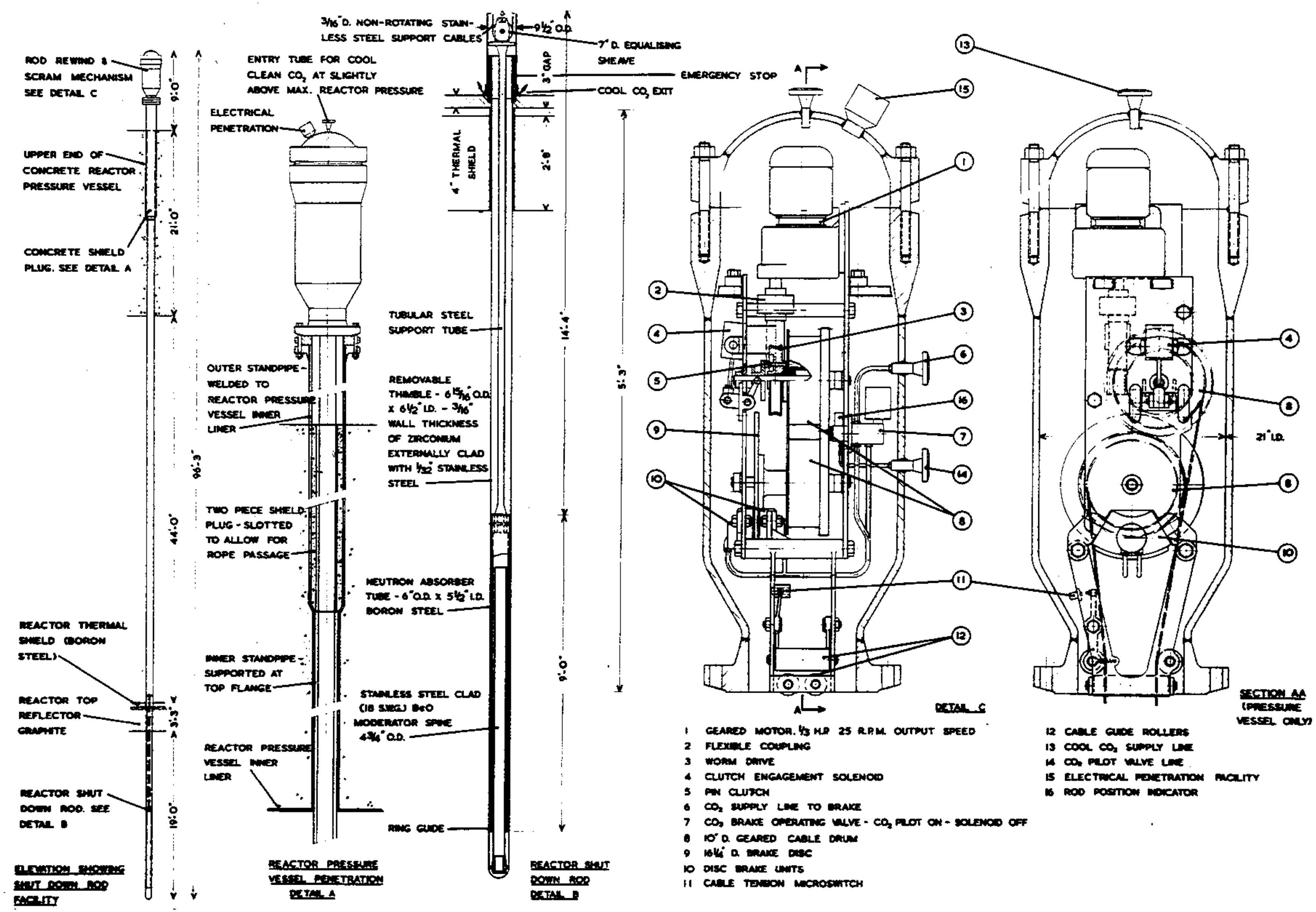


FIGURE 19. SHUT-DOWN ROD AND DRIVE MECHANISM FOR A P.B.R.

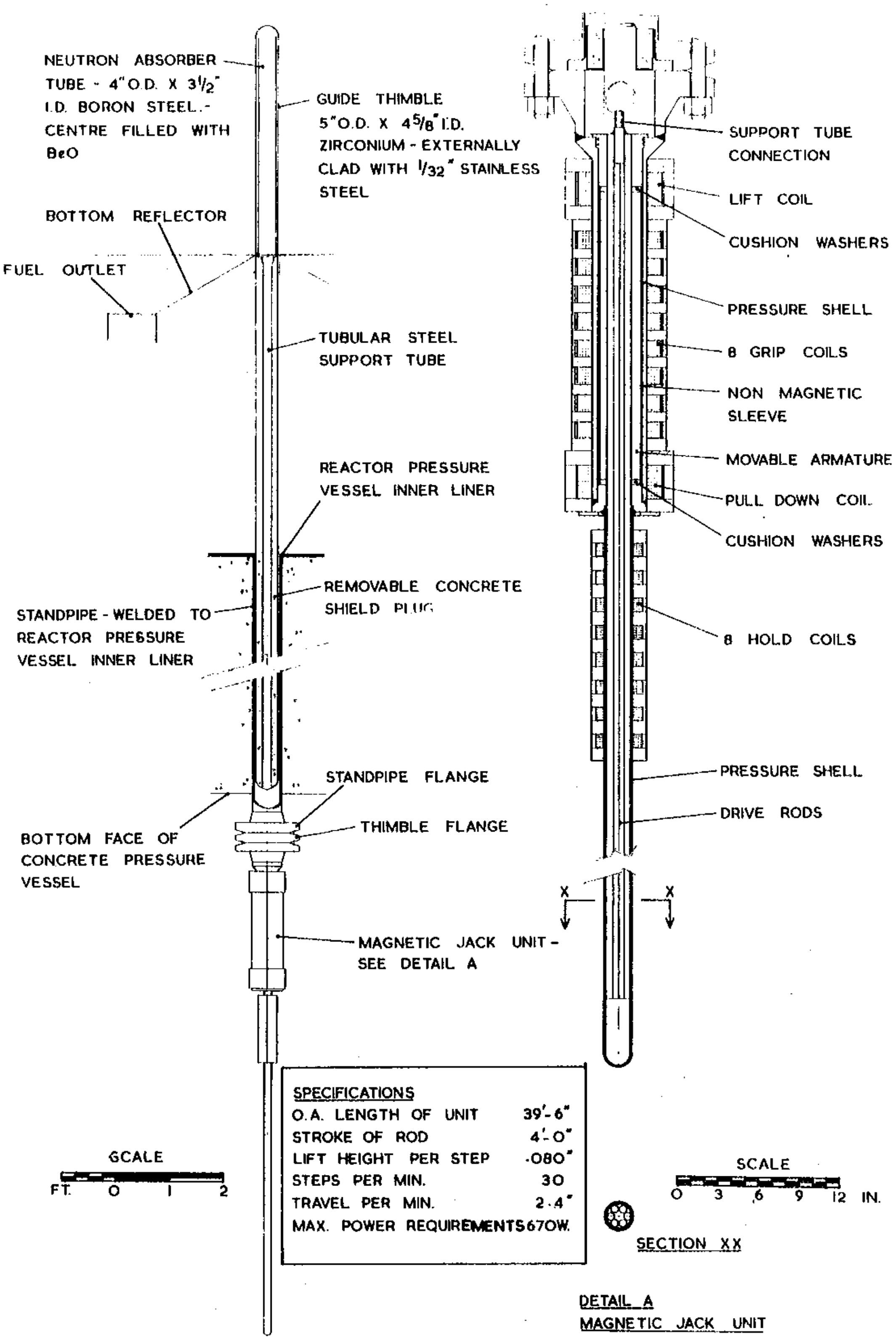


FIGURE 20. CONTROL AND SHIM ROD FOR A P.B.R.

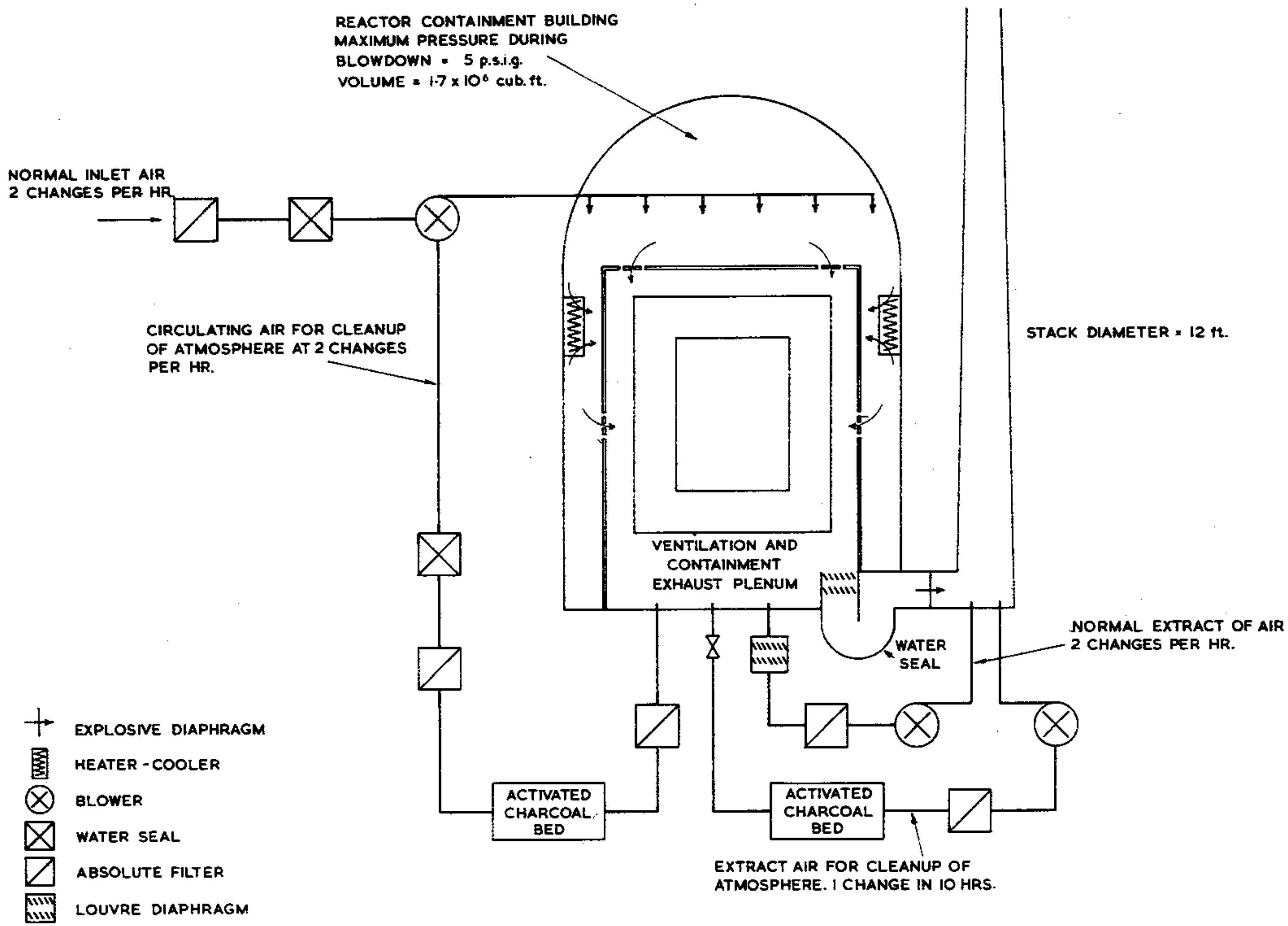
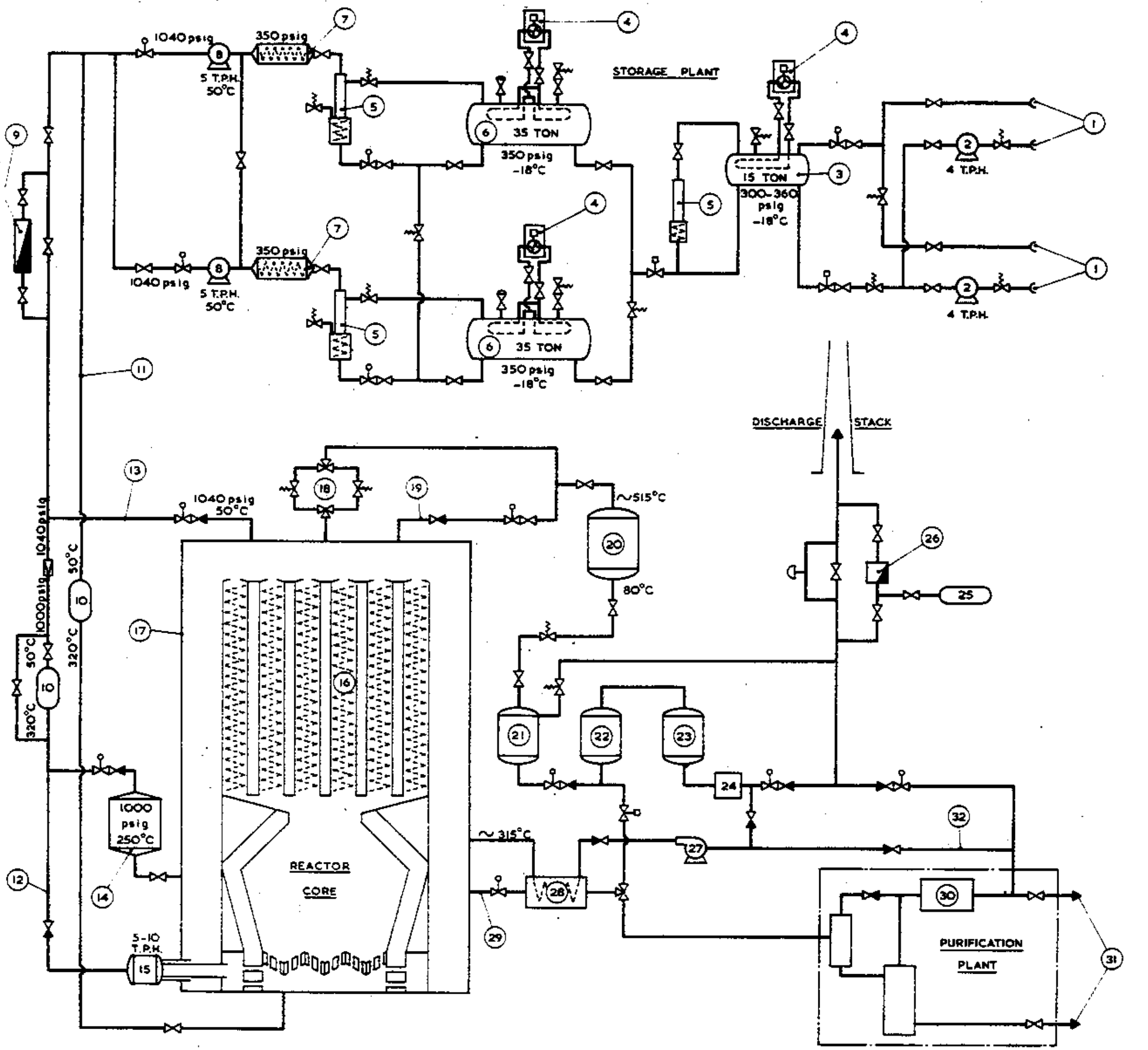


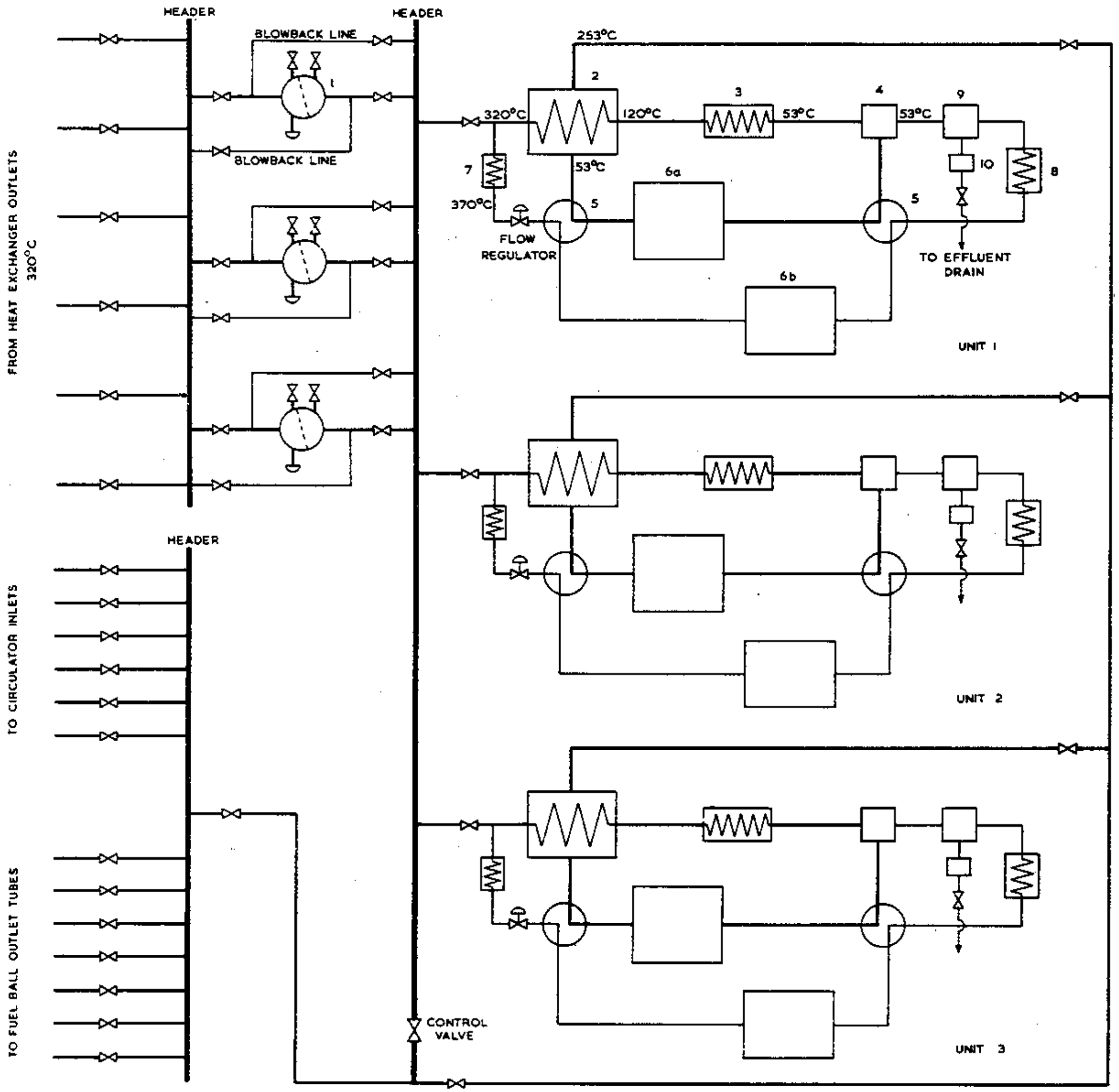
FIGURE 21. VENTILATION AND CLEAN-UP SYSTEM FOR A P.B.R.

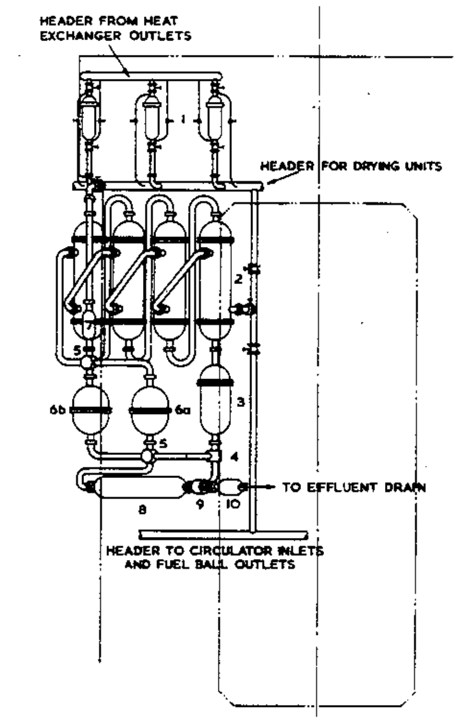


1. LIQUID CO_2 TANKER SUPPLY POINTS
2. LIQUID CO_2 TRANSFER PUMP 4 T.P.H.
3. RECEPTION TANK 15 TON 300-360 psig
4. REFRIGERATION PLANT -18°C
5. VAPOURISER UNIT
6. STORAGE TANK 35 TON 350 psig
7. SUPERHEATER UNIT 50°C OUTLET CO_2 GAS
8. GASEOUS CO_2 PUMP 5 T.P.H. 1040 psig OUTLET CO_2
9. CO_2 FLOWRATOR UNIT
10. ELECTRICAL HEATER UNIT 320°C OUTLET CO_2
11. EMERGENCY CO_2 LINE 1040 psig 320°C
12. MAKEUP CIRCUIT CO_2 LINE 1000 psig 320°C
13. CONTROL ROD SERVICE LINE 1040 psig 50°C
14. BALL HANDLING SERVICE LINE 1000 psig 250°C
15. CO_2 CIRCULATORS 5-10 T.P.H. (6 OFF)
16. HEAT EXCHANGERS
17. PRIMARY CIRCUIT CONTAINMENT VESSEL
18. REACTOR SAFETY VALVE CIRCUIT
19. NORMAL BLOWDOWN LINE
20. HEAT SINK 515°C INLET 80°C OUTLET (6 OFF) 16'-0" x 3'-6" DIA.
21. CO_2 DUMP TANK (6 OFF) 22'-0" x 4'-0" DIA 1860 ft³ TOTAL CAPACITY AT REACTOR WORKING PRESSURE
22. COARSE AND ABSOLUTE FILTER UNIT
23. ACTIVATED CHARCOAL ABSORBER BEDS
24. FILTER UNIT FOR CHARCOAL PARTICLES
25. CO_2 BOTTLE
26. STACK IODINE MONITOR
27. RECIRCULATION FAN
28. REGENERATIVE HEATER
29. BLEED CO_2 PURIFICATION LINE
30. XENON AND KRYPTON TRAPS
31. WASTE DISPOSAL LINE
32. PURE CO_2 RECIRCULATION LINE

STOP VALVE
CHECK VALVE
PRESSURE RELIEF VALVE
SOLENOID CONTROL VALVE
PILOT OPERATED RELIEF VALVE
AIR RELEASE VALVE
PRESSURE REDUCING VALVE
BURSTING DISK
CHANGEOVER VALVE

FIGURE 22. FLOW CHART FOR CARBON DIOXIDE STORAGE AND PURIFICATION





ARRANGEMENT OF CO_2 DRYING UNIT RELATIVE TO CONCRETE PRESSURE VESSEL (ONE UNIT ONLY SHOWN)

1. BYPASS FILTER
2. REGENERATIVE HEAT EXCHANGER BANK
3. GAS/WATER HEAT EXCHANGER
4. VENTURI JUNCTION
5. FOUR WAY VALVE
6a. DRYING UNIT - DRYING
6b. DRYING UNIT - REACTIVATING
7. GAS HEATER
8. CONDENSER
9. SEPARATOR
10. CONDENSATE TANK

FIGURE 23. ARRANGEMENT OF CARBON DIOXIDE BYPASS FILTERS AND DRIERS

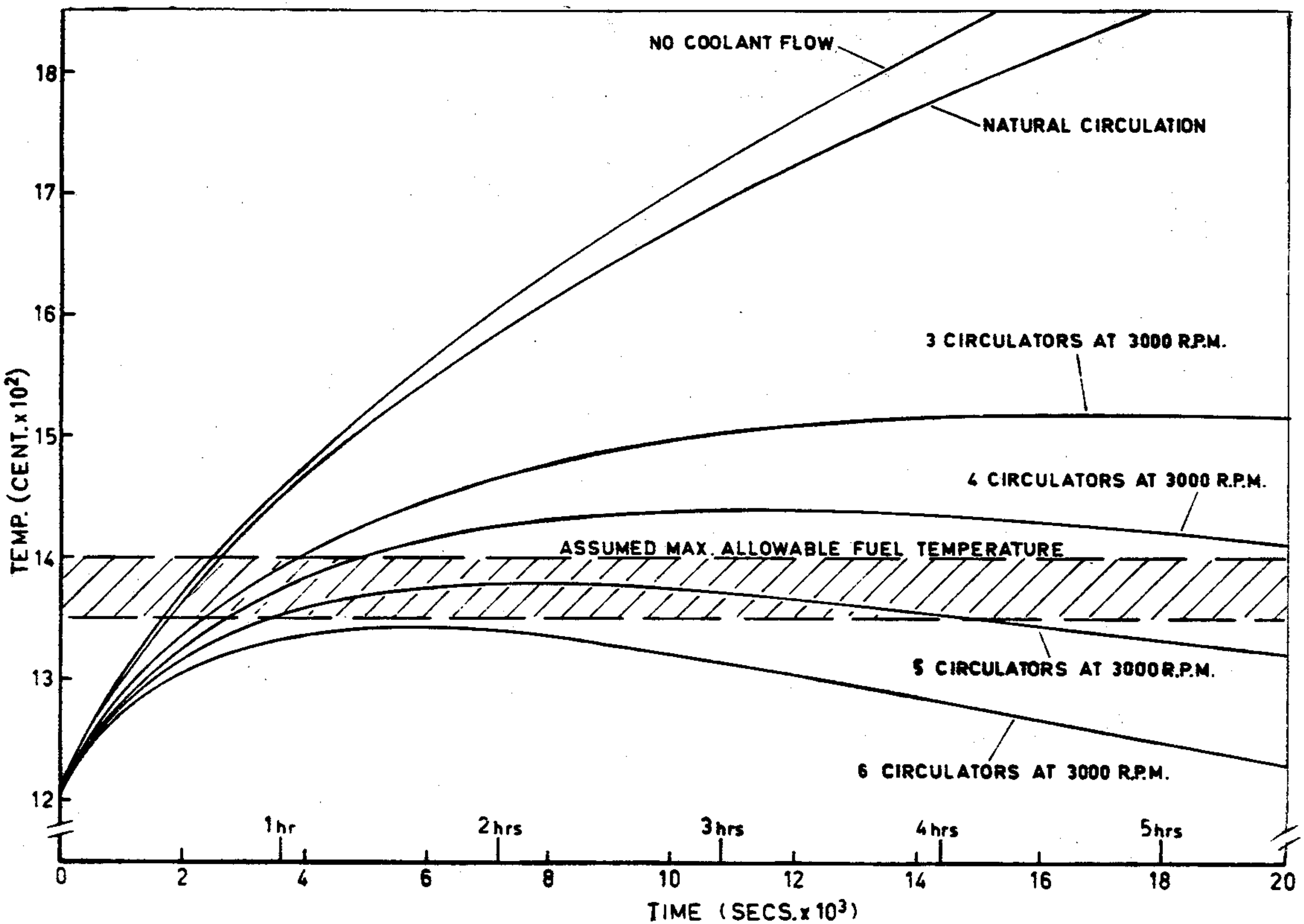


FIGURE 24. RESULTS OF CALCULATIONS OF FUEL TEMPERATURE FOLLOWING A DEPRESSURIZATION ACCIDENT

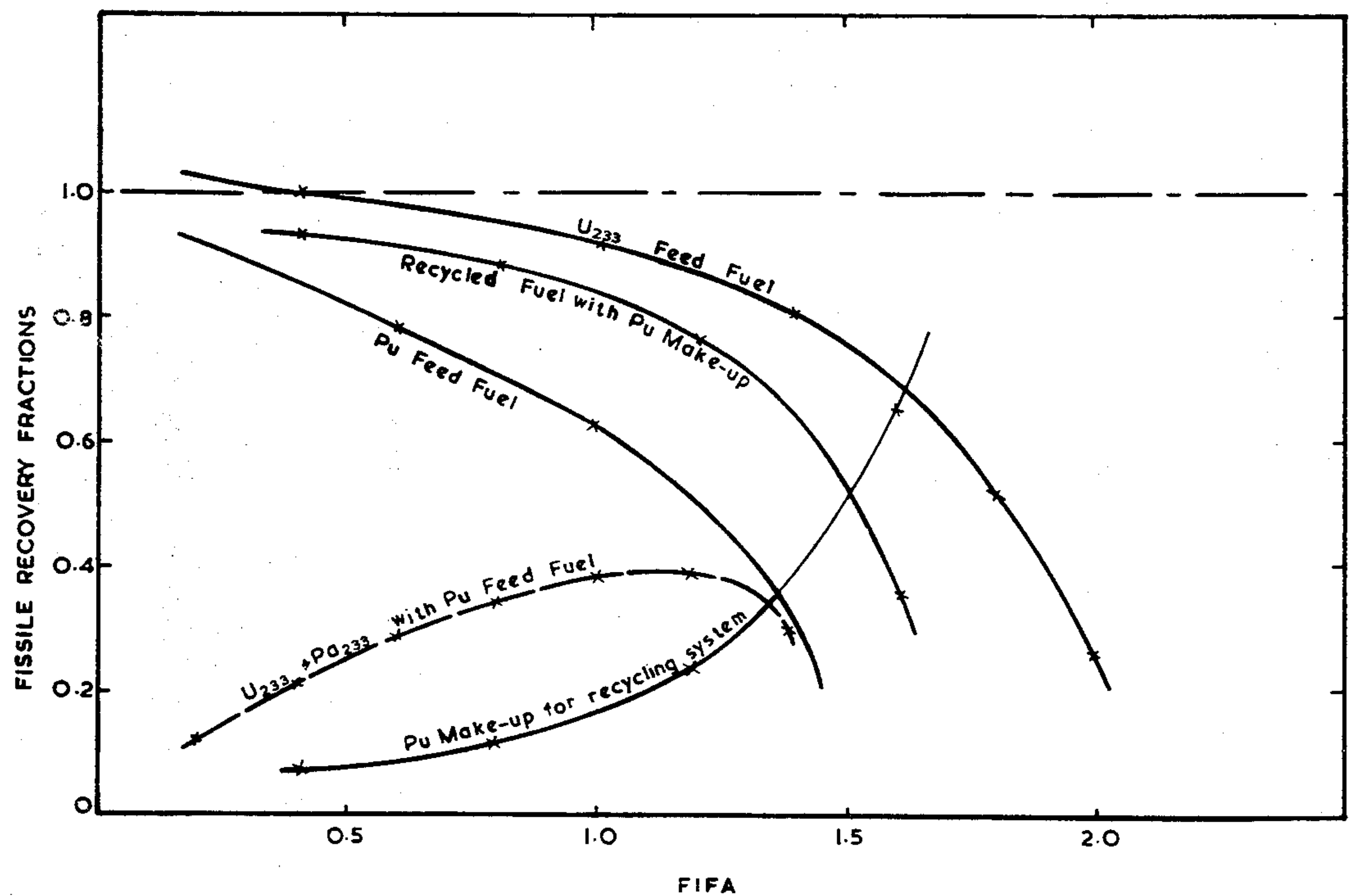


FIGURE 25. ESTIMATED FUEL RECOVERY FRACTIONS FOR THE REFERENCE DESIGN P.B.R. OPERATING AT VARIOUS F.I.F.A.

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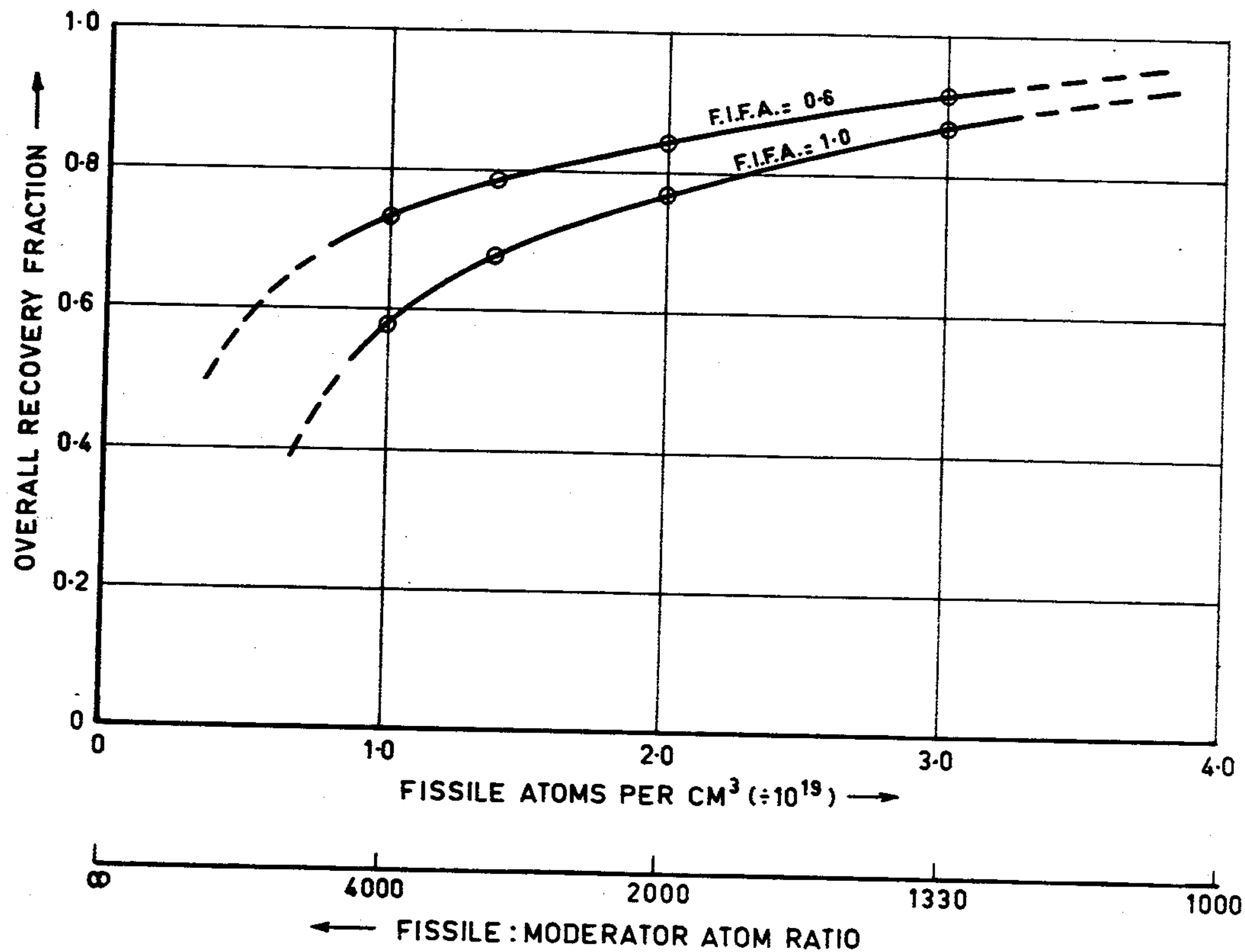

FIGURE 26. VARIATION OF RECYCLE FUEL RECOVERY FRACTIONS WITH BURNUP AND MODERATOR RATIOS

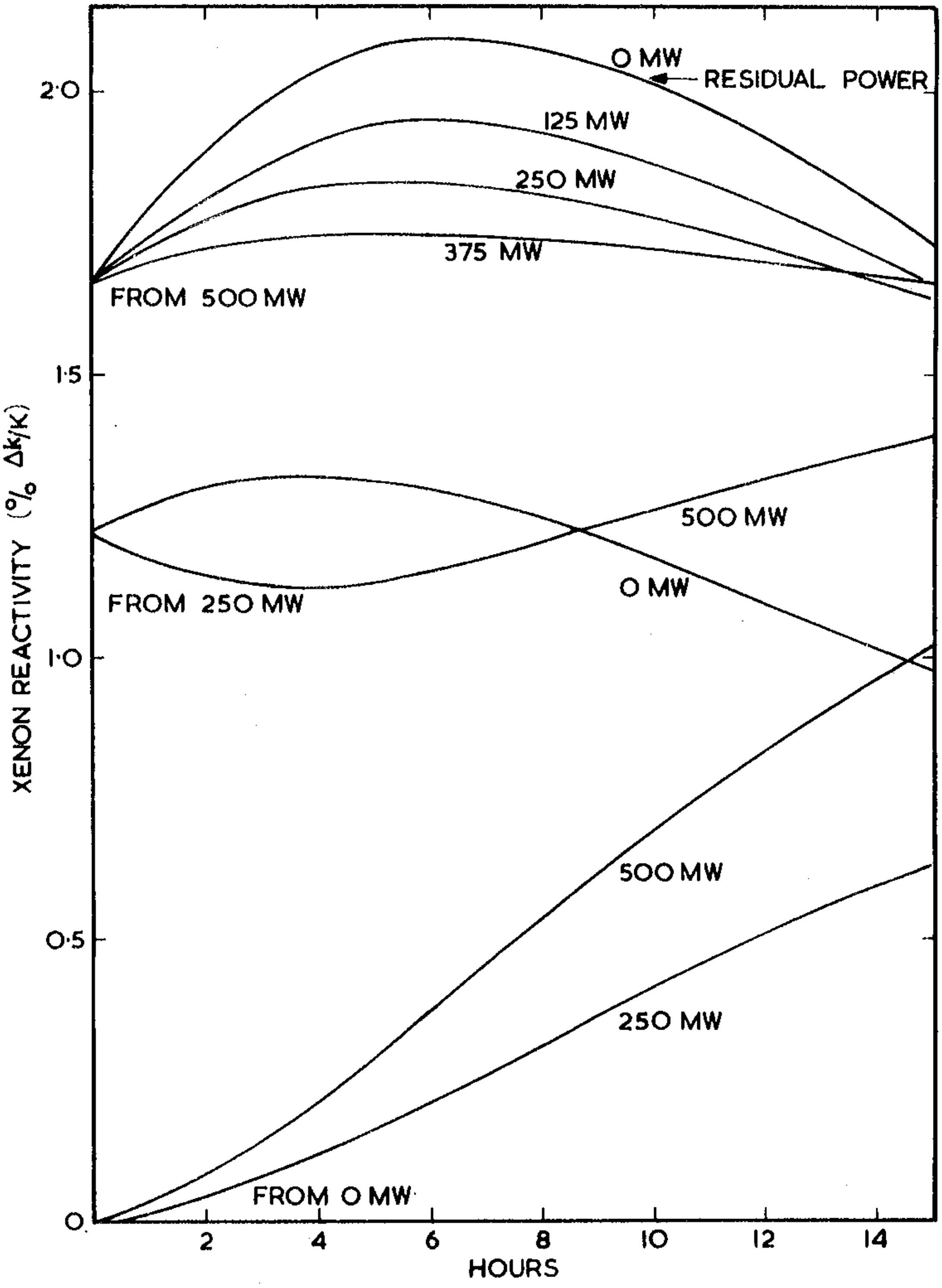

FIGURE 27. XENON TRANSIENTS FOR STEP POWER CHANGES IN THE P.B.R.

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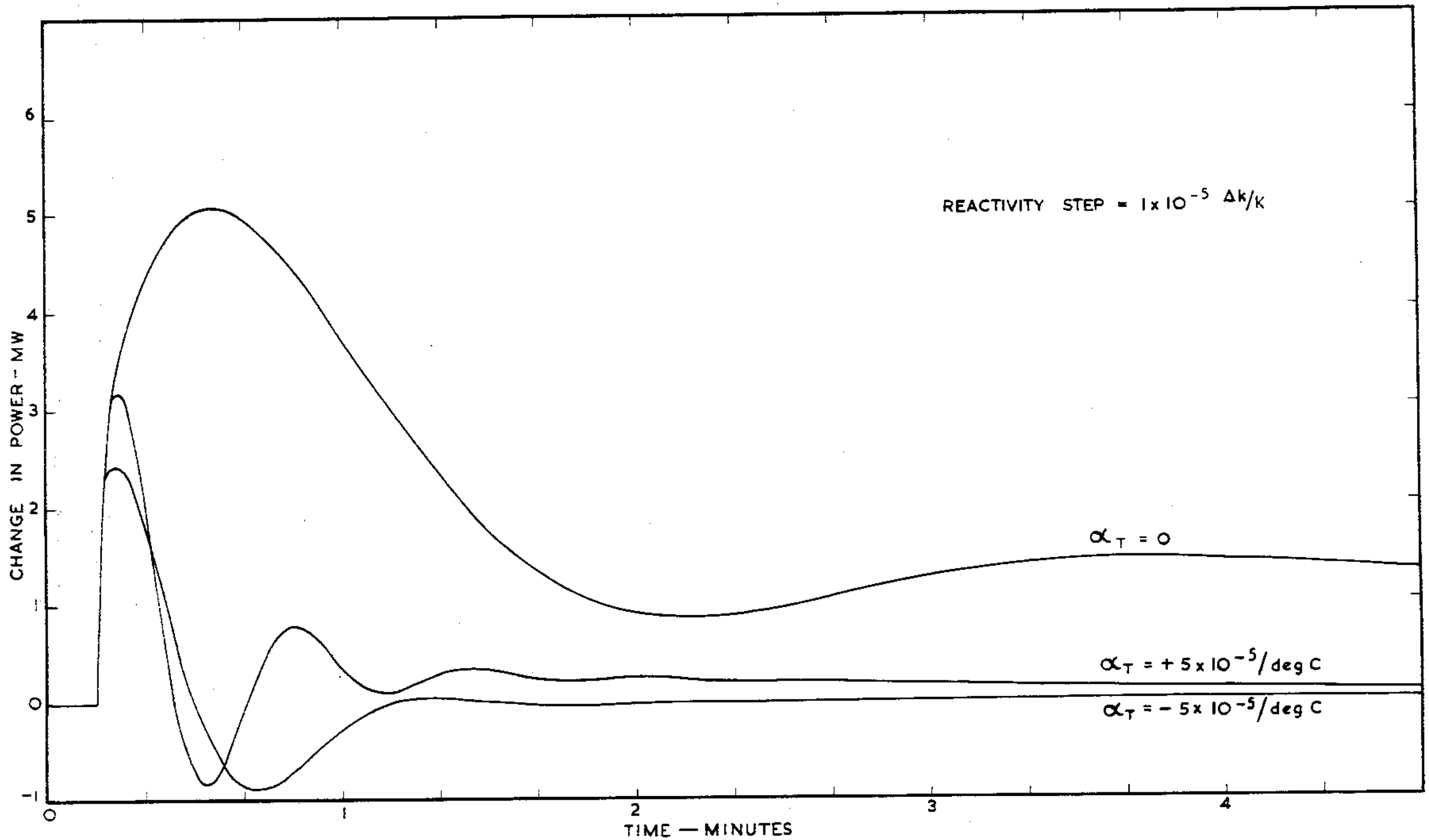

FIGURE 28. POWER VARIATIONS WITH STEP REACTIVITY DISTURBANCE IN THE P.B.R.

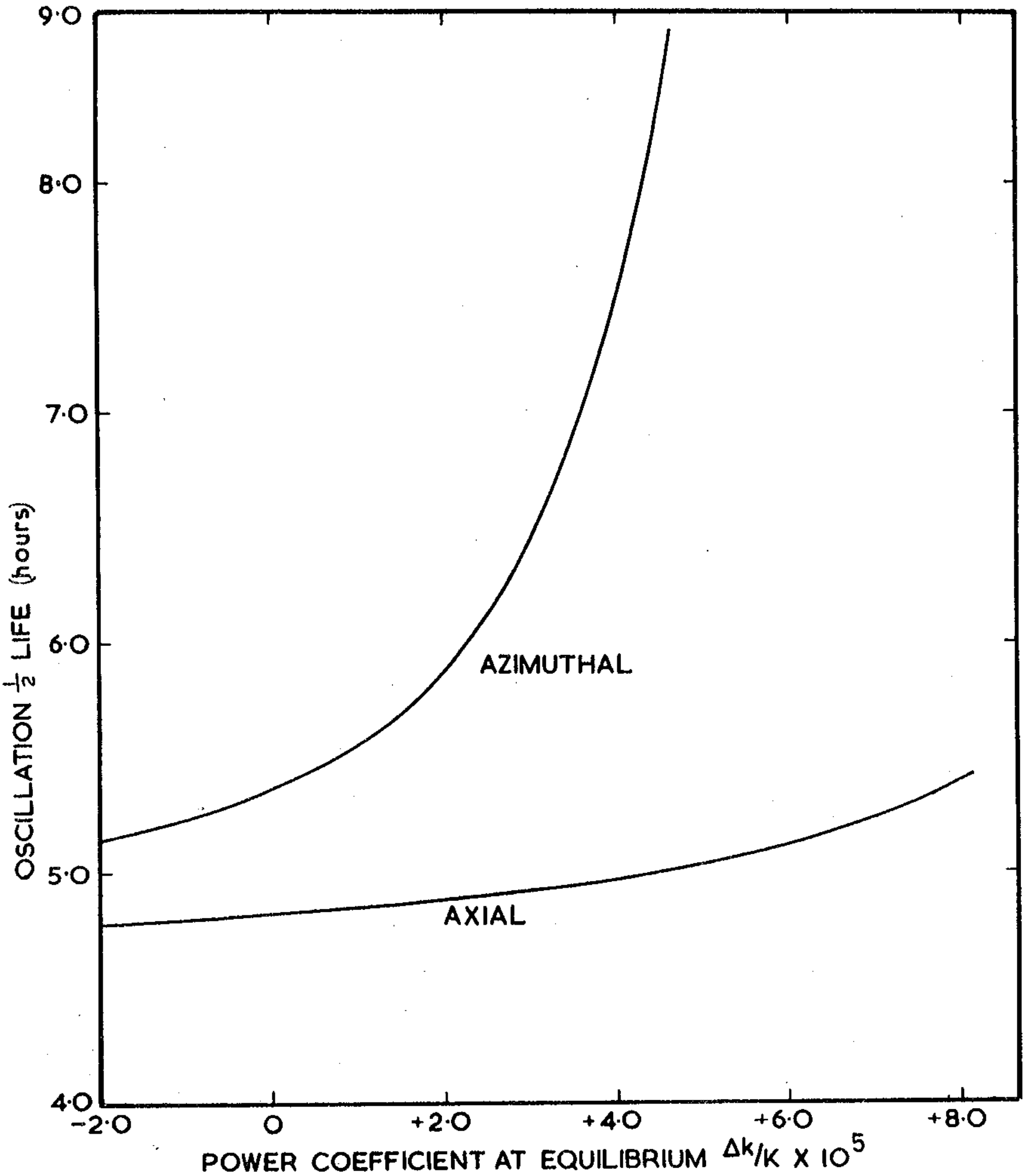

FIGURE 29. HALF LIFE OF SPATIAL NEUTRON FLUX OSCILLATIONS IN THE P.B.R. CORE

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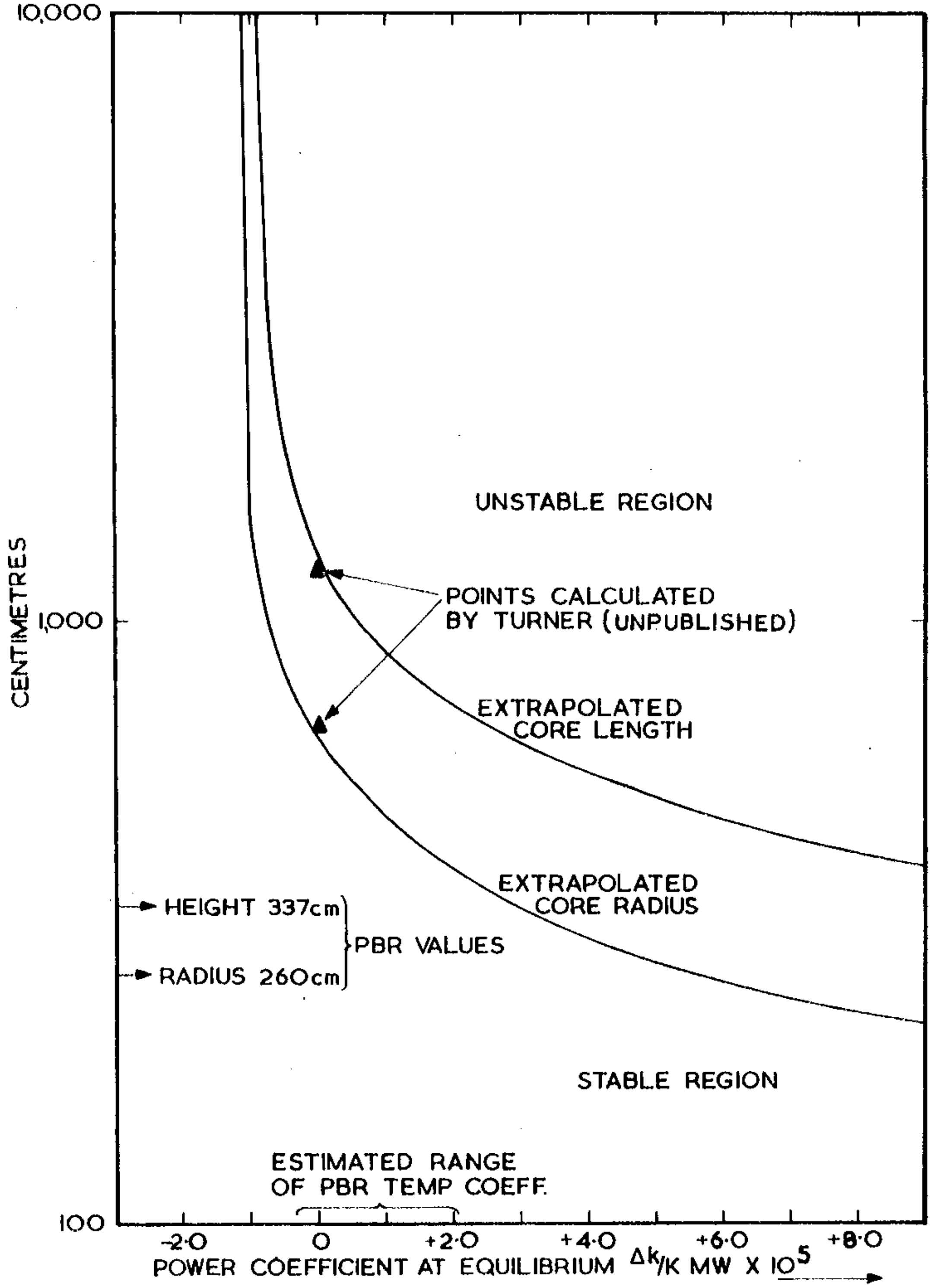

FIGURE 30. DIMENSIONS OF UNFLATTENED P.B.R. CORES HAVING NEUTRALLY STABLE SPATIAL POWER OSCILLATIONS IN THE AXIAL AND RADIAL DIRECTIONS

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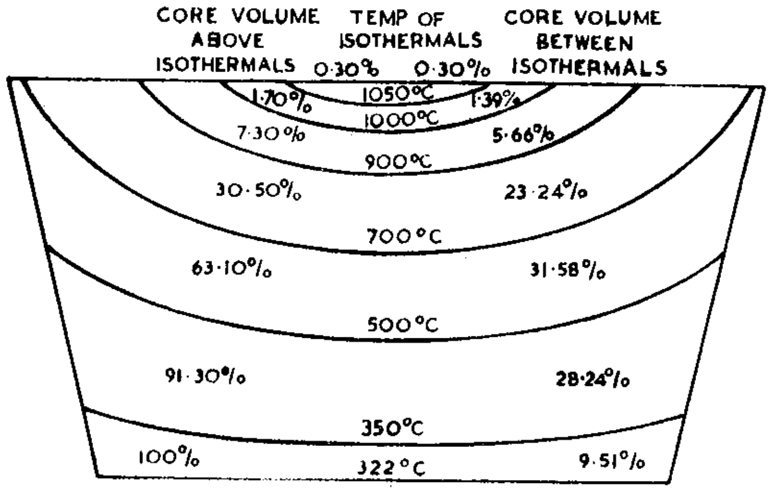
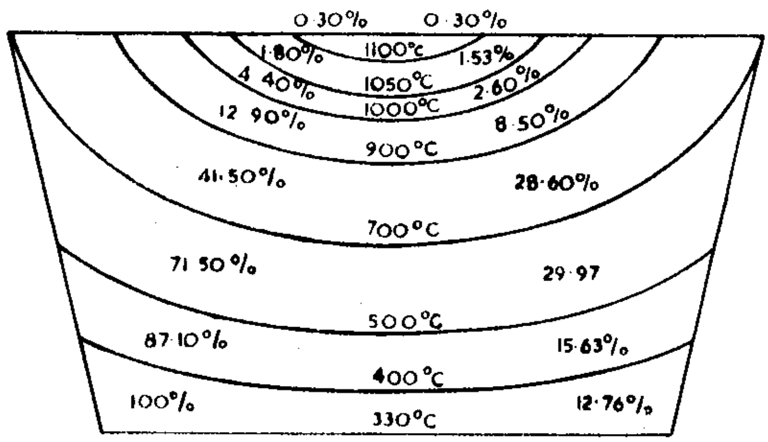
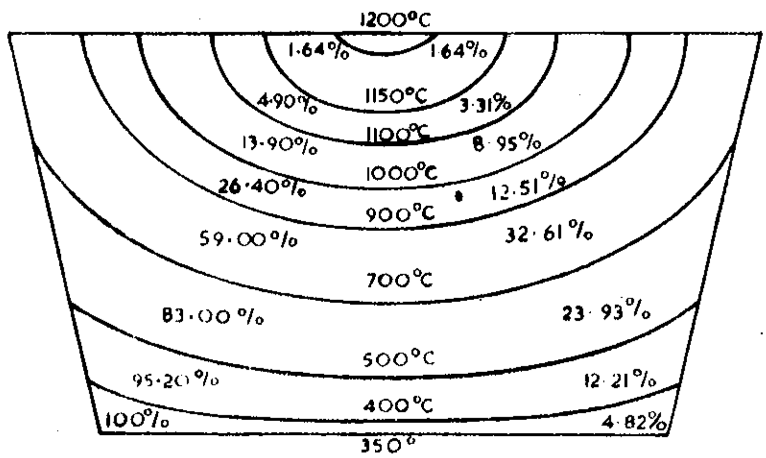


CO_2 COOLANT ISOTHERMALS ON CORE CROSS SECTION



FUEL BALL AVERAGE SURFACE TEMPERATURE ISOTHERMALS



FUEL BALL PEAK SURFACE TEMPERATURE ISOTHERMALS

FIGURE 31. VARIATION OF FUEL ELEMENT AND COOLANT TEMPERATURE THROUGHOUT THE REFERENCE P.B.R. CORE

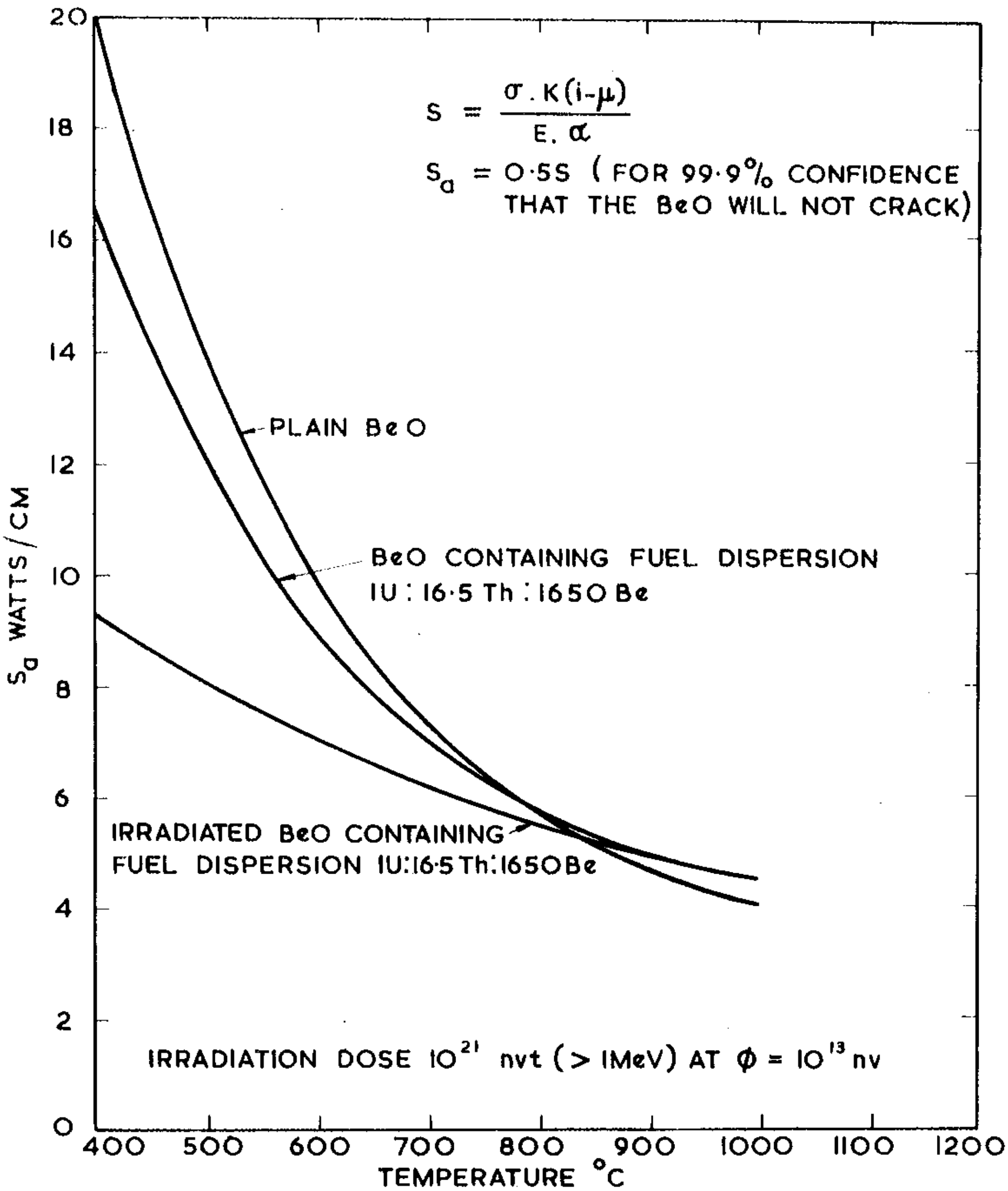


FIGURE 32(a) VARIATION OF THERMAL STRESS RESISTANCE OF BERYLLIA WITH TEMPERATURE

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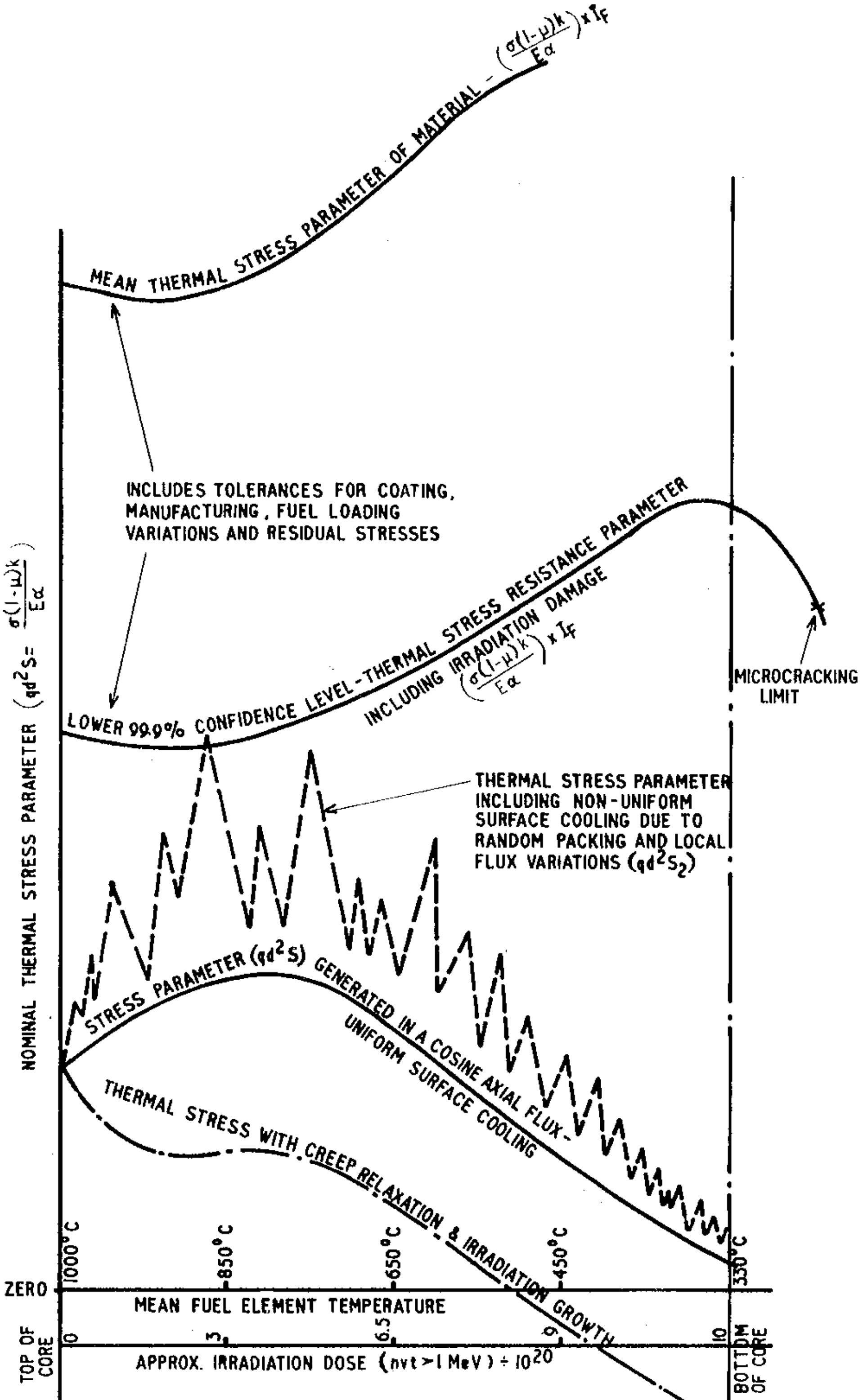

FIGURE 32(b) THERMAL STRESS IN A P.B.R. FUEL ELEMENT PASSING THROUGH THE CORE

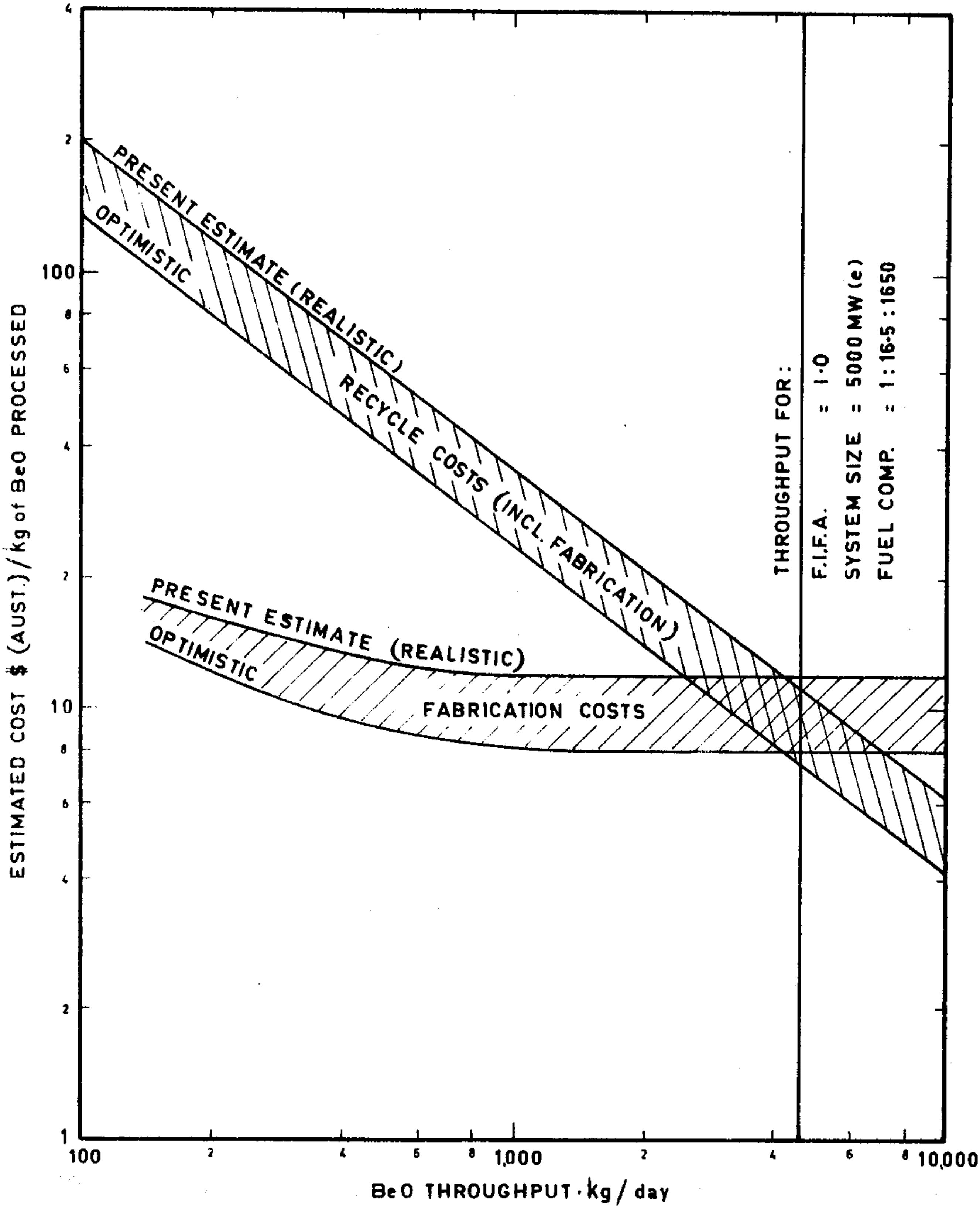

FIGURE 33. H.T.G.C.R. RECYCLE AND FABRICATION COSTS

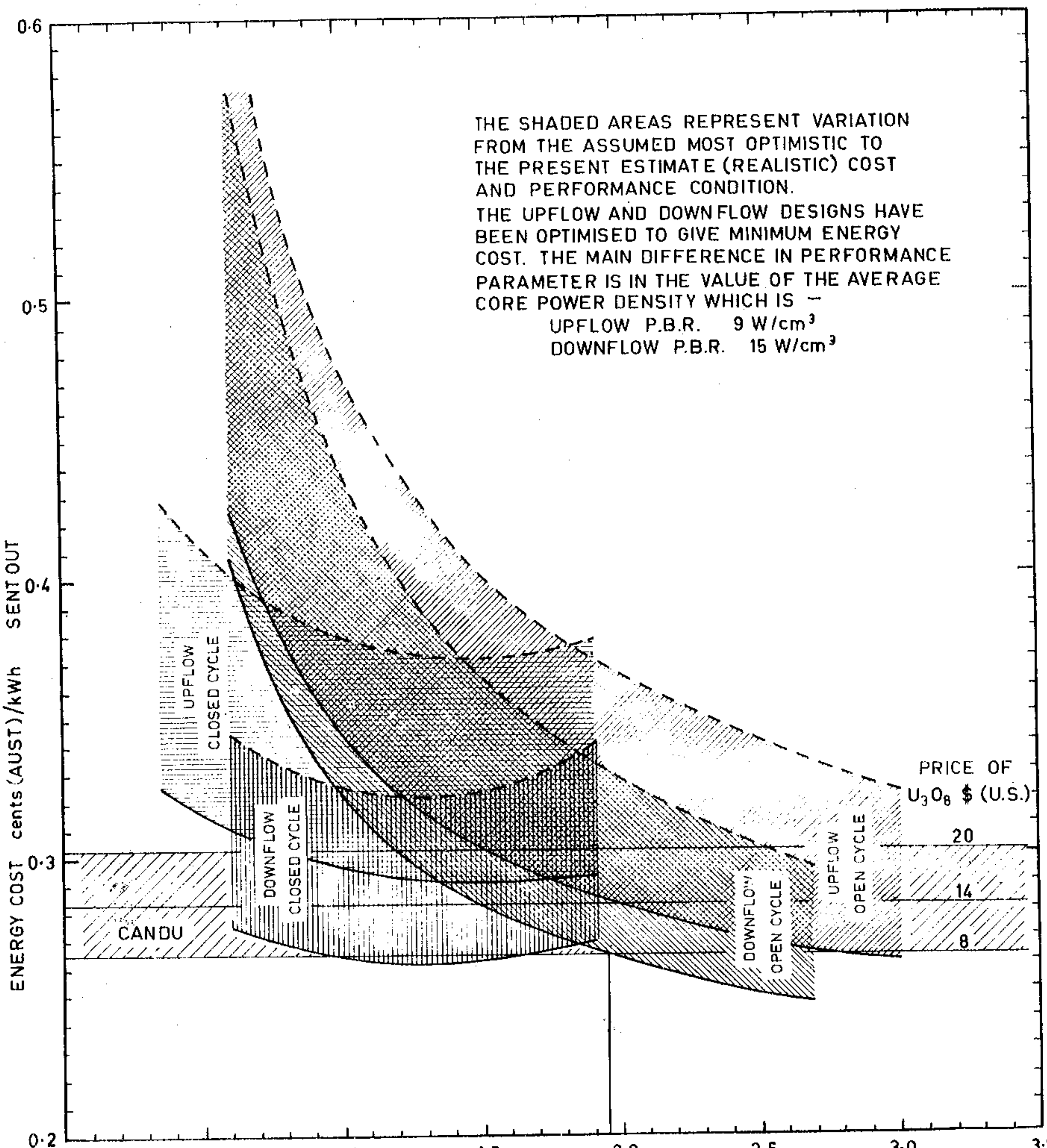

FIGURE 34. ENERGY COSTS v. BURNUP FOR TWIN 500 MWe P.B.R. POWER STATIONS

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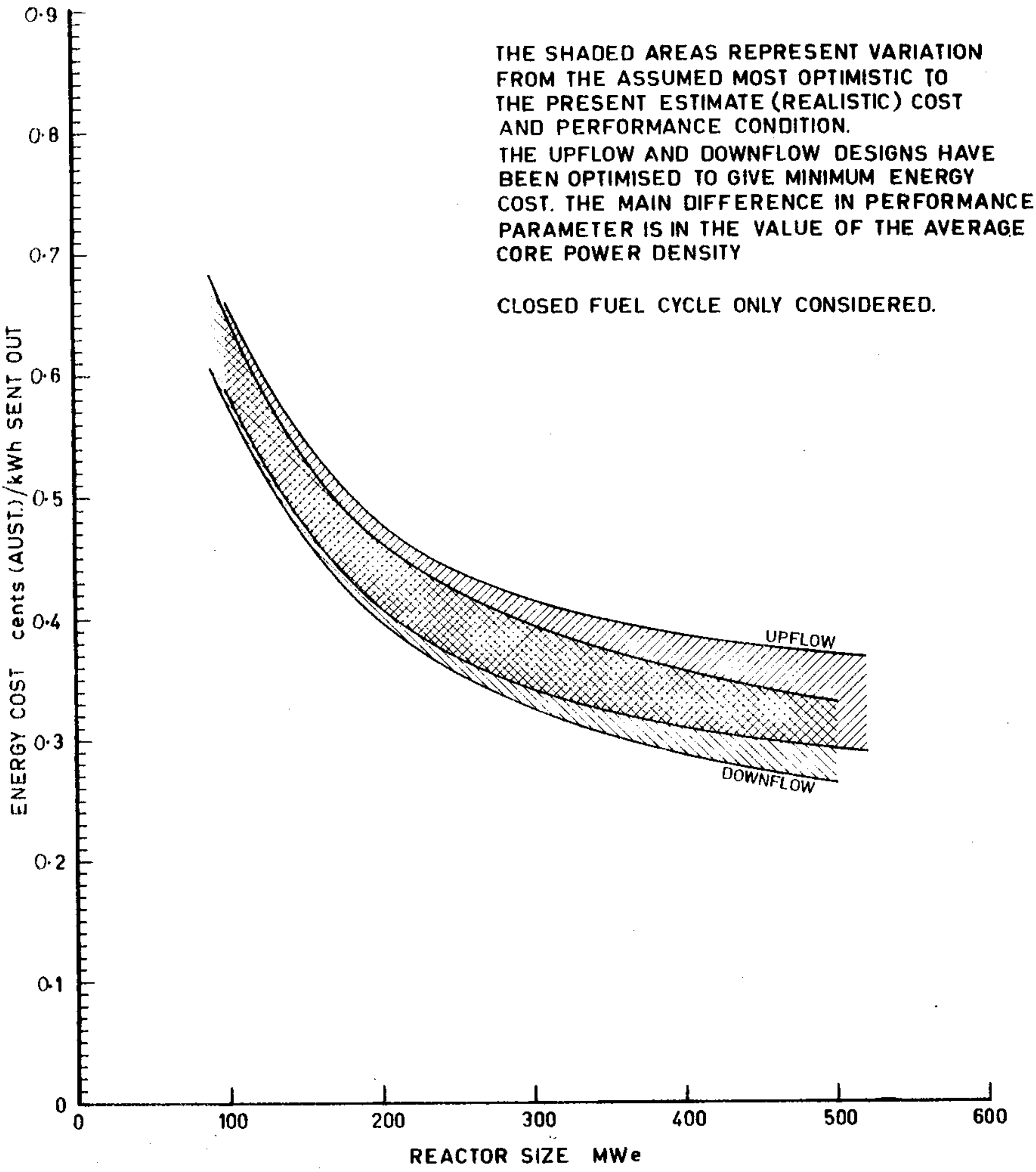

FIGURE 35. GENERATING COST FOR VARIOUS SIZES OF TWIN P.B.R. POWER STATIONS

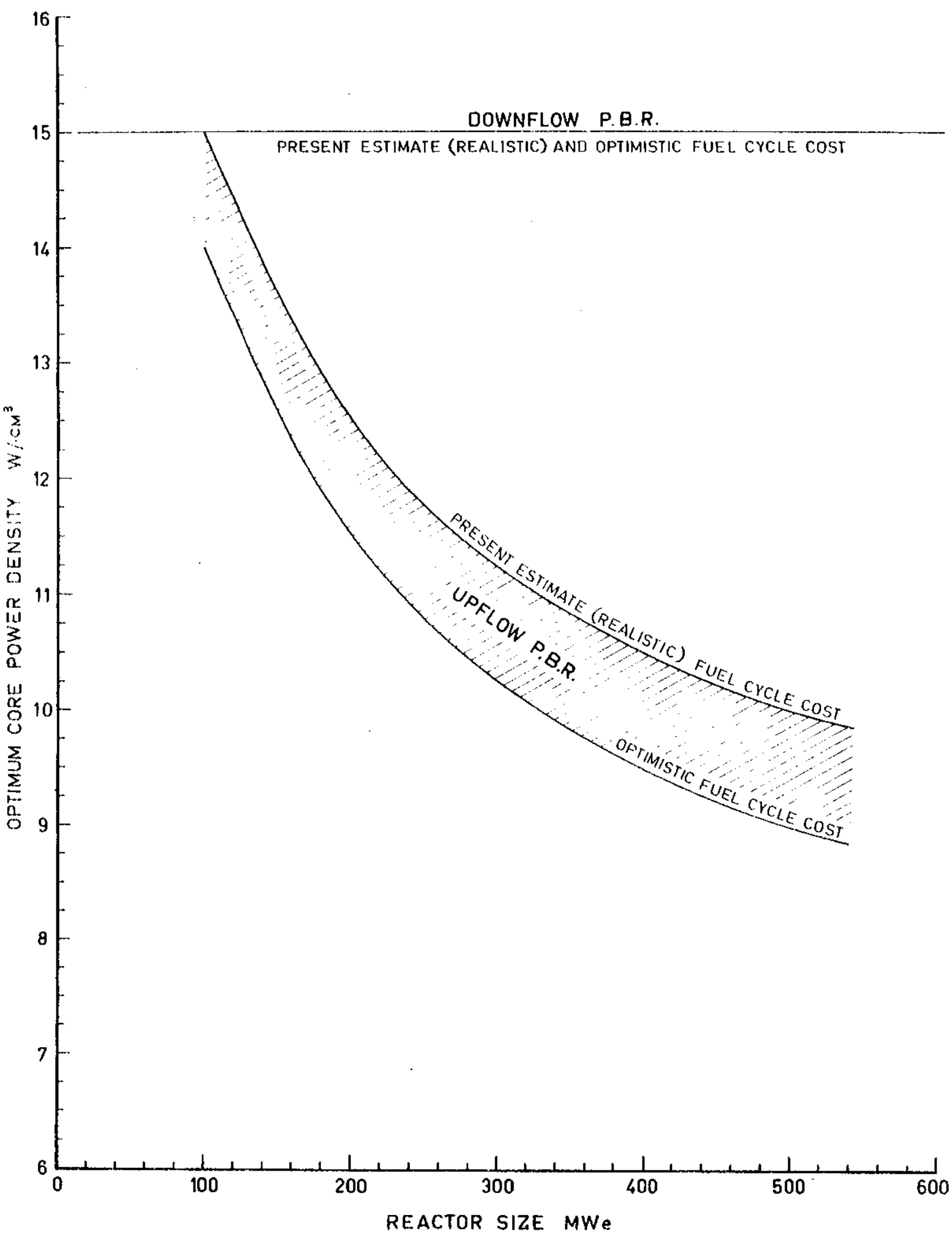

FIGURE 36. VARIATION OF OPTIMUM POWER DENSITY WITH SIZE OF P.B.R.