AN INTEGRATED MARINE PROPULSION SYSTEM UTILISING TRIGA^{RM} FUEL

BY

P.A. BEELEY, J.M. BRUSHWOOD, C. COLLINS, M.J. FREEMAN, P.J. HAMILTON, A. NEWELL, N. SCHOLES, A. THOMPSON

(Nuclear Department, Defence College of Electro-Mechanical Engineering HMS SULTAN, Military Road, Gosport, PO12 3BY, UK*)*

G. MANACH, J-P. MONNEZ
(Ecole des Applications Militaires de l'Energie Atomique, Cherbourg, France)

This is an edited version of the paper that was presented at the *International Conference on Advanced Power Plants, Pittsburgh, PA, USA, 13-17 June 2004.*

Introduction

The feasibility of using a 15 MW_{th} *General Atomics TRIGA*™ core as an autonomous marine power supply was described at ICAPP'03[1]. This study concluded that the design of a small (15 MW_{th}) nuclear reactor for use as a marine power source was feasible but the incorporation of a large reserve coolant tank and hydrodynamic ports might introduce constraints, especially with respect to flow through the hydrodynamic ports during natural circulation. An obvious natural progression was to consider an integral plant using the same type of fuel and the present paper describes such a plant with the minimum power required to realistically meet propulsion and hotel electrical load requirements. The use of *TRIGA*™ type fuel in high power research reactor applications, such as the 14 MW_{th} plant operated by the Institute of Nuclear Research, Pitesti, Romania and the proposed up rating to 21 MW_{th}, is well documented[2].

As with the previous study, reactor physics modelling was carried out using the SERCO Assurance[3] computational codes WIMS 8A and MONK 8B and thermal hydraulics modelling was carried out using COBRA, TRACPFQ and in-house calculations. Some specific constraints were imposed on the design in order to meet, at least qualitatively, safety principles and selected safety criteria consistent with modern nuclear safety justification and these are addressed in the various sections below. As with most design studies, the final proposed design for an integral plant is based on sound engineering compromise between the reactor physics and thermal hydraulic requirements. The key parameters of the proposed IMPS plant are given in Table 1 and a schematic shown in (FIG.1).

The shielding, radiological protection and safety aspects of the final design were also examined. The SERCO Assurance[3] Monte Carlo General Radiation Transport code MCBEND 9E was used to design the primary shield and to assess the dose rates, and was used to study the effects of various shielding combinations on total crew dose (using different thicknesses for the inlet and outlet shield tank walls and the bulkhead). Optimisation studies of shielding weight were also undertaken by positioning of the control room in front of the reactor compartment and by limiting occupancy in the engine room. Shield weight was also optimised by taking into account the current Basic Safety Objective.

TABLE 1 - *Key IMPS Parameters*

Primary Circuit	**Value**
Thermal Power	50 MW
Pressure	16 MPa
Core flow rate	450 kg s^{-1}
Core inlet temperature	563 K
Core outlet temperature	583 K
Core power density	47.8 MWm^{-3}
Average linear power	20.4 kWm^{-1}
Max. linear power, hot pin	64.1 kWm^{-1}
Av. linear power, hot pin	29.9 kWm^{-1}
Thermal efficiency	25 %
Secondary Circuit	**Value**
Pressure	3 MPa
SG tubes outside area	161 m^2
Heat transfer coefficient	4817 W $m^{-2}K^{-1}$
Height of tube bank	1.74 m
Number of tubes	950
Tube outer diameter	0.015 m

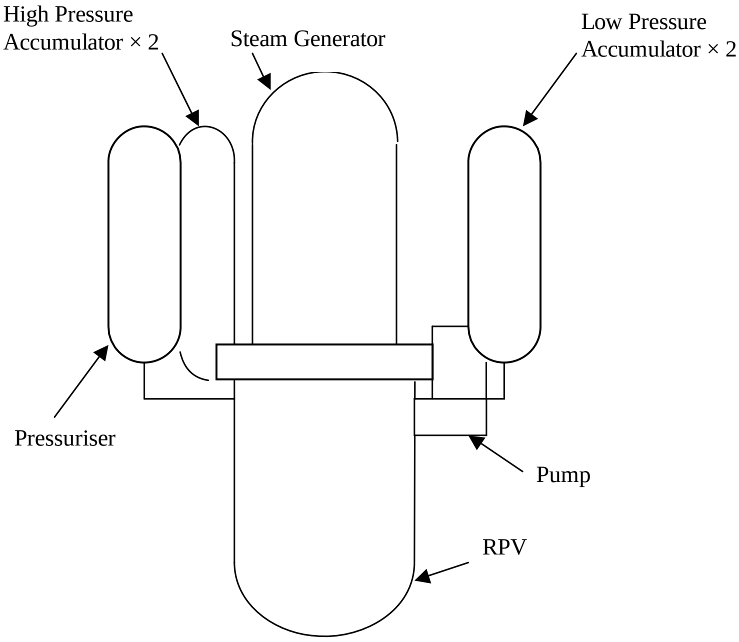

FIG.1 - INTEGRATED PLANT LAYOUT, SHOWING SG ABOVE RPV, PRESSURISER AND ECCS ACCUMULATORS

Reactor Physics

To estimate the required through-life thermal power output and the maximum reactor power the predicted operational profile, typical hotel and propulsion electrical loads were assumed. This study concluded that the necessary maximum reactor power was 50 MWth. To achieve the desired operational lifetime of 1082 weeks a total through-life thermal power of 49025 MW days or 1044 full power days (FPD) would be required.

Computational Methods

Deterministic neutronic calculations were carried out using WIMS 8A, extensive use was also made of the Monte Carlo code MONK 8B for benchmarking WIMS results and shielding design using MCBEND 9E used a source term derived from the WIMS calculations. In all cases the required nuclear data, with the exception of Erbium and bound Hydrogen in Zirconium Hydride, were taken from the standard WIMS JEF 2.2, 172-group library[4].

Three distinct computational schemes were developed:

(i) for parametric studies a 2D calculation based on the characteristics solver WCACTUS.

(ii) for whole core analysis a 3D calculation using the diffusion theory solver WSNAP.
(iii) for benchmark purposes a number of MONK models.

In brief the WIMS calculation routes (i) and (ii) used the WHEAD/WMIX modules to generate macroscopic cross sections using the equivalence treatment in the resonance region. For the important resonance absorbers 235U, 238U and 167Er the more detailed resonance treatment using the sub-group[5] method was applied.

Subsequently the multicell collision probability module WPERSEUS and the flux solver WPIP were used to generate a 172-group flux solution. This flux solution together with the previously generated cross sections were then used by the energy condensation module WCONDENSE to generate new 7-group data. Having now generated few group constants the high versatile characteristics method solver WCACTUS[6] was used to calculate a 7-group flux solution and the estimated $k\infty$.

To examine power variations the module WEDIT was used to generate pin-by-pin power maps. At later stages in the study when through life reactivity variations were studied the modules WDIFF and WBRNUP were inserted and a cyclic calculation implemented.

In the final stage of this study whole core analysis was required. This was achieved using the general geometry 3D diffusion theory solver module WSNAP. The previously described 2D calculation route was to generate appropriate few group constants. However, for this study a simple 2-group energy condensation was used and geometric detail was removed using module WSMEAR to produce spatial averaged homogeneous materials that could then be used by WSNAP.

Module Design

A design constraint had been imposed that standard ‘commercial off the shelf’ TRIGA™ fuel pins should be used in this study; consequently the effects of varying pin radius, cladding, etc were not studied.

Initially, open infinite lattices of these pins were studied, a previous study[2] having indicated that to maintain the desired under-moderated condition the maximum acceptable moderator to fuel ratio, expressed as the ratio of hydrogen (both in the water and fuel) to 235U, was 100:1. This implied that the maximum permissible pin pitch would be of the order of 21 mm. The variation of $k\infty$, determined using WCACTUS, is shown in (FIG.2). Also shown in (FIG.2) are the MONK ‘benchmark’ results. Excellent agreement was observed for all pin pitches less than 18 mm. The divergence between the WIMS and MONK results for larger pin pitches was attributed to inadequate ‘meshing’ in the WCACTUS module; this effect was also seen when ‘benchmark’ whole cruciform module results were determined. The peak in $k\infty$ occurred at a pitch of 20.8 mm, corresponding to a moderator to fuel ratio of 94:1.

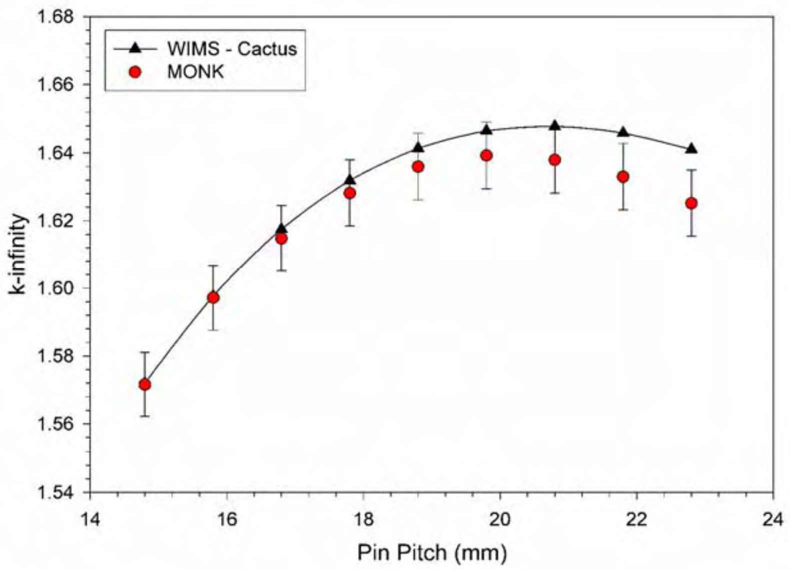

FIG.2 - VARIATION OF K∞ WITH PIN PITCH

Thermal hydraulic limitations, discussed in Section IV, limited the minimum pin pitch to 15.5 mm. A pin pitch of 16.3 mm was adopted, although larger pitches were acceptable with respect to their neutronic characteristics. A further design constraint of a maximum core diameter of 1250 mm also influenced the final choice of pin pitch.

In light of the proposed marine application the shock resistance of any proposed core design was given a high priority. Consequently a closed 6 by 6 pin canned module with 2 mm Incolloy 800 sidewalls was adopted. The reactivity effect of the introduction of such large neutron absorbing components was studied; typically a 2 mm module can reduce reactivity by approximately 2500 pcm.

In addition to incorporating module sidewalls to enhance shock resistance a cruciform shaped control rod was adopted. This shape allowed the internal control rod guide plates to act as braces within the module and consequently further enhance the structural integrity. The final module design is shown in (FIG.3).

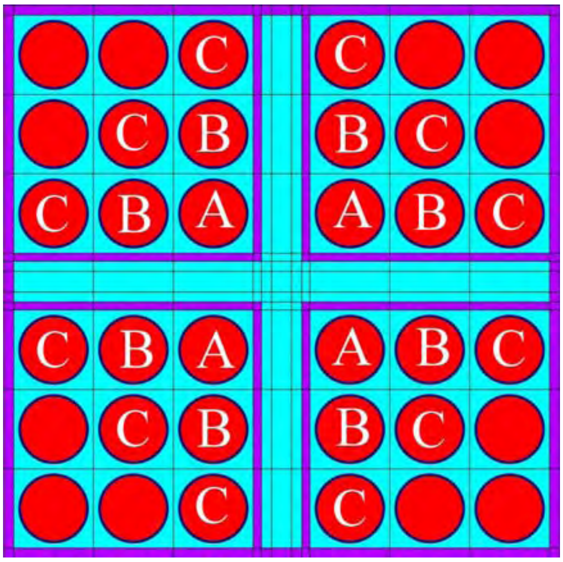

FIG.3 - FINAL CRUCIFORM MODULE DESIGN

In the initial module design all fuel pins were un-poisoned. The local power peaking factors (PPF) produced by the water filled voids in unrodded modules, with all materials at their nominal operating temperatures, were investigated. After considerable iteration in both poison loading and zoning the optimised solution adopted was four pins (3 wt% Er) in position A and eight pins (2 wt% Er) pins in position B. The PPF profile across the optimised module varied from 0.93 to 1.05.

Due to the integrated nature of the plant design the available space for control rods and mechanisms above the reactor core was severely restricted. Consequently, the number of control rods was minimized. Potential control rod materials considered were hafnium, an alloy of Silver/Indium/Cadmium and Incolloy clad boron carbide. Of these materials initial scoping calculations indicated that only the use of boron carbide rods would be able to achieve the desired cold (300K) shutdown margin of – 2000 pcm.

Core Design

The proposed core (FIG.4) consists of 68 fuel modules (red, blue and green) arranged to give a configuration with a maximum diameter of 1280 mm, slightly greater than the design aim of 1250 mm but considered acceptable. Also shown in this Figure are the 4 banks of control rods (1, 2, 3 and 4). Surrounding the entire fuel region is a 1310 mm diameter core barrel (not shown).

In the first design iteration all modules had the same erbium poison distribution. Both start of life (SOL) and end of life (EOL, corresponding to 1044 FPD) PPF were determined. The worst-case PPF, (1.43) was, as expected, located at the core centre. Whilst this value was within the acceptable limits set by thermal hydraulics the large variation in PPF seen across the core caused concern.

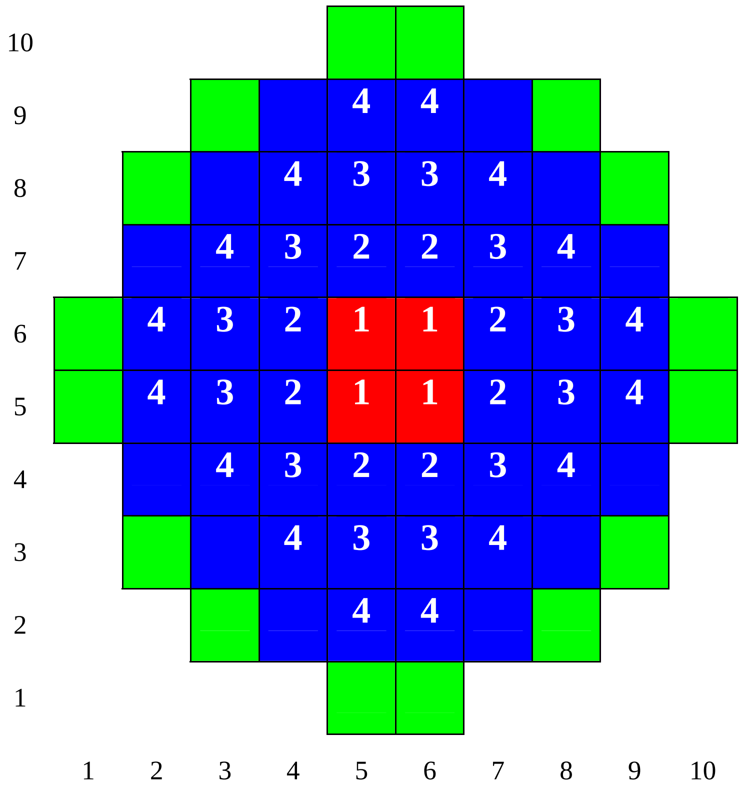

FIG.4 - REPRESENTATIVE CORE SLICE

The option of the inclusion of lumped poisons in the module sidewalls was discounted on both potential manufacturing difficulties and the likely reduction in structural integrity.

The final scheme adopted was: no erbium present in the 16 outermost modules (green in FIG.4); to retain the original 3 wt% Erbium (blue in FIG.4); and to introduce a further twelve 2 wt% erbium poisoned pins (position C in FIG.2) into the four central modules (red in FIG.4). The resulting PPF factors at SOL and EOL are shown in (FIG.5).

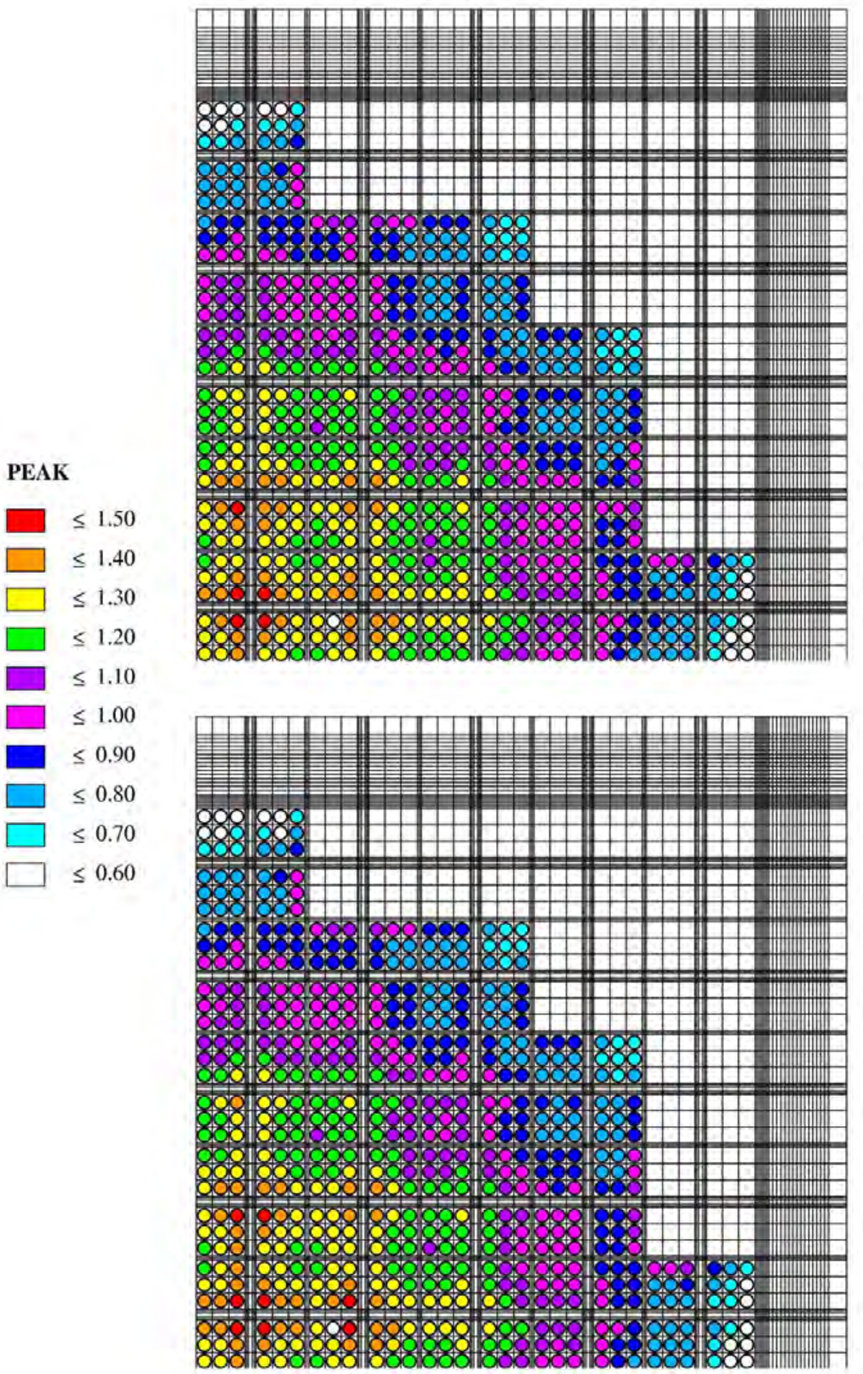

FIG.5 - POWER PEAKING FACTORS AT SOL (TOP) AND EOL (BOTTOM) FOR FINAL CORE CONFIGURATION

Whole Core Design

Finally, whole core properties such as the expected start of life critical rod positions, the axial power factors and the fuel and moderator temperature coefficients were determined. Thermal hydraulic constrains had indicated that the fuelled length required was 980 mm.

To study these, a 3D model was developed in WSNAP. In order to use this module it was necessary to smear all materials to produce a homogeneous material that could then be used to fill a 3D xyz mesh. Extensive use of the MONK code was made at this stage to assess the effect of this homogenization. At the time of writing only a 300 K library was available for MONK and consequently 'benchmarking' of the WSNAP model could only be conducted for the cold condition (i.e. all core materials at 300 K). For a full unrodded core the WSNAP model estimated a keff of 1.259; this compared well with the MONK estimate of keff 1.256 ± 0.002.

Although conducted in cold conditions the good agreement observed between the two models gave confidence that prediction under hot conditions would be reliable. Using the WSNAP model but now with all core materials at their nominal 'hot' operating temperatures (moderator temperature = 573 K and fuel temperature = 825 K) a burn up calculation was carried out to determine the core lifetime; these results are shown in (FIG.6).

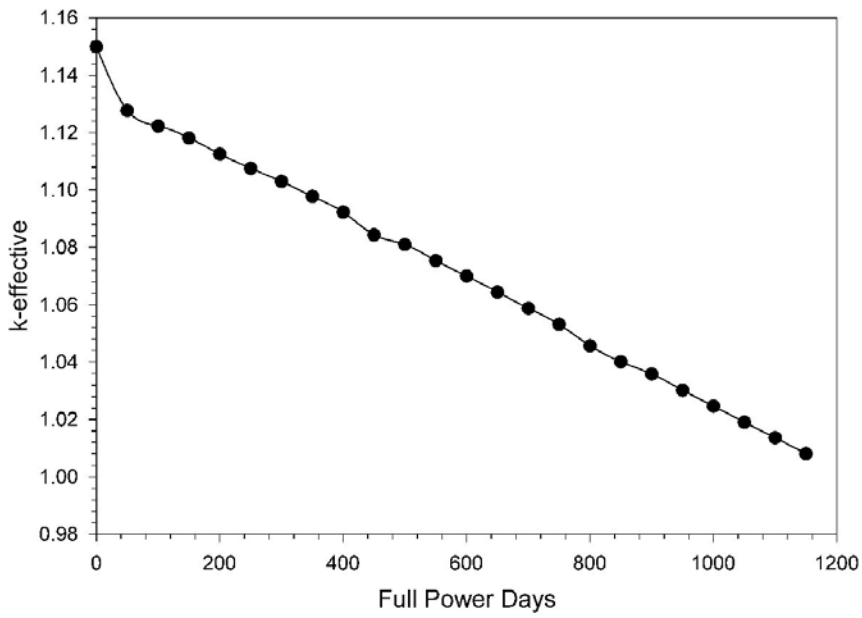

FIG.6 - UNRODDED THROUGH LIFE REACTIVITY VARIATION

With respect to control rods, to meet the design safety principles, a constraint was imposed that the minimum cold shutdown margin should be at least -2000 pcm. The final scheme selected used 4 groups of rods (labelled 1, 2, 3 and 4 in FIG.4) that would be moved as banks. Full insertion of all of these rods gave a keff of

0.9682 (a shutdown margin of –3284 pcm); again this compared well with the MONK estimate of keff of 0.9639 ± 0.002.

Control rod worth curves for all 4 banks of control rods were determined for the hot operational conditions (moderator temperature = 573 K and fuel temperature = 825 K). These are shown in (FIG.7). The initial estimated critical position (ECP) was 220 mm rod withdrawal.

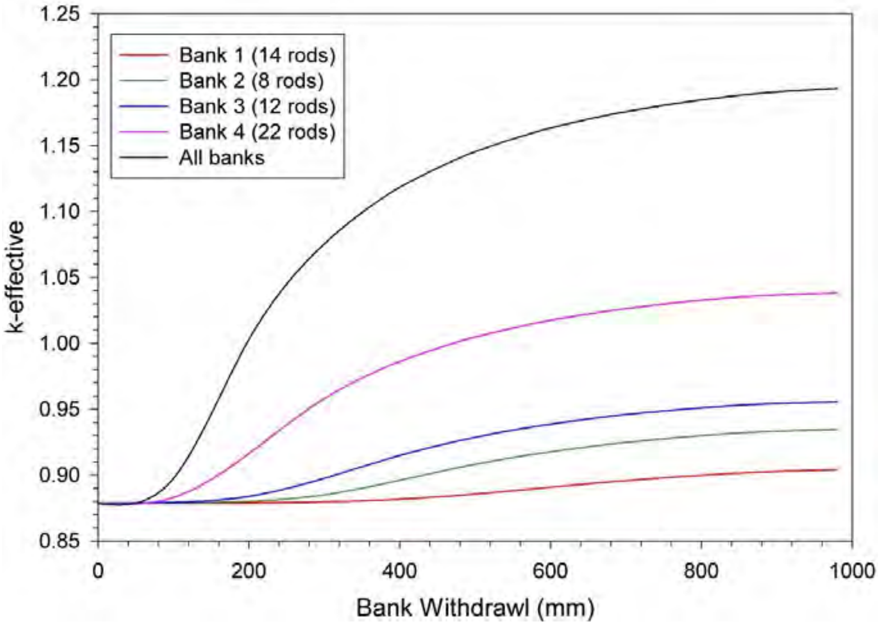

FIG.7 - SOL ROD WORTH CURVES

Start of life axial and radial flux profiles were determined for normal hot operating conditions (ECP = 220 mm) for a variety of axial and radial slices through the core.

Finally start of life fuel and moderator temperature coefficients of reactivity were determined. The temperature coefficients were determined over a 50 K range about the nominal fuel temperature of 825 K and moderator temperature of 573 K respectively. The fuel coefficient of -5.4 pcm K-1 agreed well with both previous work[2] and calculations by *General Atomics*[6] after taking into account the variation in fuel composition. The moderator coefficient was determined to be -6.1 pcm K-1.

Shielding

The fission gamma and neutron source term, referred to as the primary source, used in MCBEND was determined from the reactor physics calculations and smeared over the entire core region. A secondary source of high-energy ^{16}N gammas was defined and smeared over the entire pressuriser region. These sources were used to calculate both the gamma and neutron dose rates outside the primary shield. Variance reduction techniques were employed in order to optimise the operation of MCBEND. (FIG.8) shows an elevation of the design, indicating the core and the primary shield.

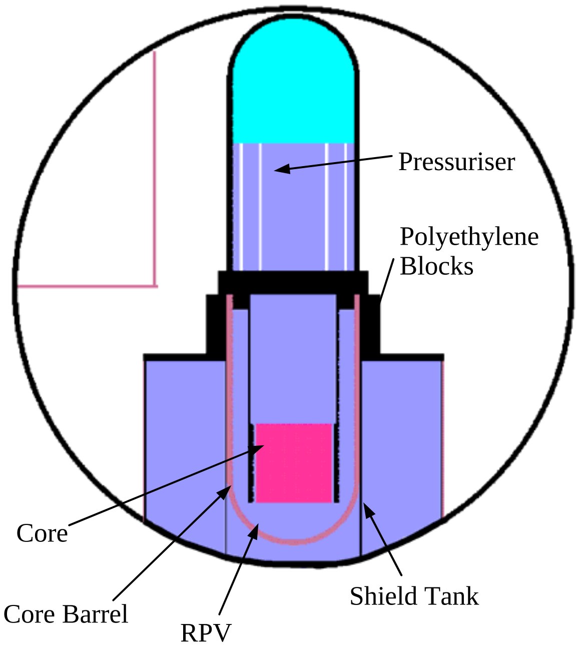

FIG.8 - ELEVATION OF THE CORE AND PRIMARY SHIELD

Neutron dose rates were found to make a very minor contribution to the total dose rate, due to the large volume of water surrounding the core and the RPV, and total dose was almost entirely due to gammas. (FIG.9) and (FIG.10) show how the primary and secondary gamma dose rates vary radially through the primary shield.

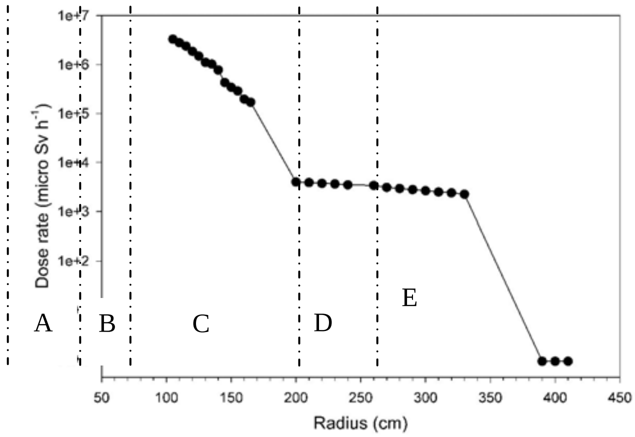

FIG.9 - PRIMARY GAMMA DOSE RATE MID CORE PLANE,
A – WATER, B – STEEL AND LEAD, C – SHIELD TANK WATER, D – STEEL,
E – AIR

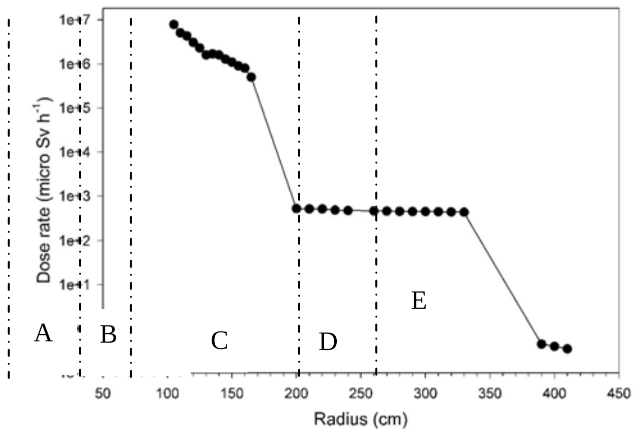

FIG.10 - SECONDARY GAMMA DOSE RATE MID CORE PLANE
A – WATER, B – STEEL AND LEAD, C – SHIELD TANK WATER,
D – STEEL AND E – AIR

Thermal Hydraulics

The general arrangement of the plant is indicated in (FIG.10), showing the single steam generator (SG) mounted above the reactor pressure vessel (RPV). The closure head of the RPV also acted as the tube sheet for the SG (FIG.11) and a single pump circulated the primary coolant. An external pressuriser was used. The integrated plant configuration allowed some power generation under natural circulation and improved loss of coolant accident (LOCA) protection compared with a dispersed plant. Calculations of the steady state performance and LOCA transients were performed.

a) Steady state core calculations

Thermal limits in the core were examined using the COBRA EN sub-channel analysis code supplied by the NEA data bank and described by Basile et al.[8]

The code was used in three modes, firstly to model a single channel, then to calculate the whole core behaviour with 49 linked channels and finally to analyse the hottest fuel rod bundle containing 16 linked sub channels. The EPRI CHF correlation[8,12] was selected from those available in the code and no hot channel factors were applied to temperature or heat flux. The thermal constraints were maximum fuel temperature less than the lower safety limit for TRIGA fuel and Departure from Nucleate Boiling Ratio (DNBR) greater than 1.5 for simple cosine power profiles or greater than 1.3 for calculated power profiles. These were applied at 120% of full power, assumed to occur during a transient. From[13] the lower safety limit is a fuel temperature of 1223K, but this assumes the clad temperature is also 1223K corresponding to an equilibrium hydrogen pressure of 1.72 MPa to burst the clad. With clad temperature less than 773 K (as in water) the rupture pressure is 25 MPa. At 50 MW (steady state full power) the maximum fuel temperature is 1030 K and hydrogen pressure is 0.08 MPa.

The single channel analysis was used in conjunction with the core physics study to choose a suitable spacing for the fuel pins. Preliminary calculations with an assumed chopped cosine axial power profile established that the DNBR was more limiting than the maximum fuel temperature. To maintain DNBR greater than 1.5 the gap between pins was set at 2.5 mm. COBRA calculations with linked channels allowed the distribution of flow to change and showed the effect on DNBR and fuel temperature. Using the flux profiles calculated by the core physics study power profiles were calculated and the results are given in Table 2.

TABLE 2 - *Limiting core conditions at core flow rate of 450 kg s^{-1}*

Power (MW)	**Min DNBR**	**Max Fuel T (K)**
60 (transient)	1.69 (bundle av)	1083 (bundle av)
60 (transient)	1.35 (hot rod)	1112 (hot rod)
50 (steady)	1.91 (bundle av)	1006 (bundle av)
50 (steady)	1.62 (hot rod)	1030 (hot rod)

The bundle average results were obtained from a 1/8th core model and the hot rod results from a sub-channel calculation for the hottest bundle. Flow distribution effects were allowed for by the channel and sub-channel calculations. The conductivity of the fuel is taken as 17.0 $Wm^{-1}K^{-1}$ from[13]. A conservatively large thermal resistance between fuel and clad is assumed which gives a temperature difference across the fuel/clad interface of 1/3rd of the temperature difference across the fuel.

The steam generator was modelled using a spreadsheet and gave the key parameters listed in Table 1. A crown shaped arrangement of U tubes (FIG.11) provided the required heat transfer area in an overall height of 3.74 m.

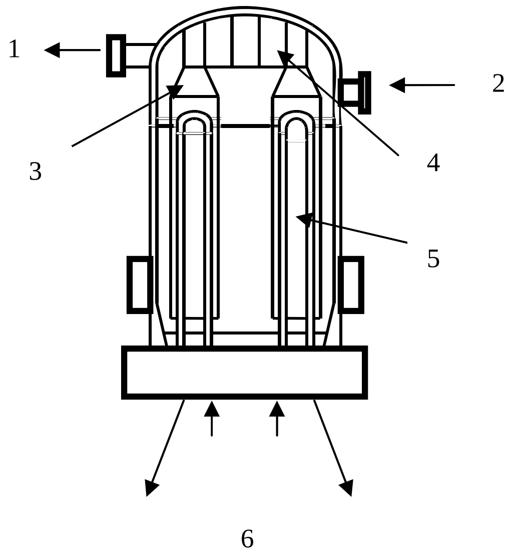

FIG.11 - STEAM GENERATOR LAYOUT
1 - STEAM OUTLET, 2 - FEED INLET, 3 – SEPARATORS, 4 - DRYERS,
5 - TUBE BANK, 6 - PRIMARY INLET AND OUTLET

b) Steady state primary circuit calculations

The diameter and number of tubes was chosen to fit the plan area of the tube-sheet. An overall heat transfer coefficient was then calculated and the heat transfer area of the SG was found for 50 MW power, average primary temperature of 573 K and secondary pressure of 3 MPa. The height of the tube bundle followed from the heat transfer area and number of tubes. The heat transfer coefficients were found from the Dittus and Boelter correlation inside the tubes and initially assuming a constant wall superheat of 5.0 K on the tube exterior. The TRACPFQ steam generator portion of the primary circuit model was then used to obtain the final SG heat transfer coefficients using the Chen correlation for the tube outer surface. No fouling factors were applied.

The complete primary circuit was modelled using the TRACPFQ code from the NEA data bank described by Basile et al[9]. (FIG.12) shows the division of the system into cells and the component connections. The return leg from the SG included a pump that was omitted for natural circulation flow. Pumped flow calculations allowed the power of the pump to be estimated as 45kW.

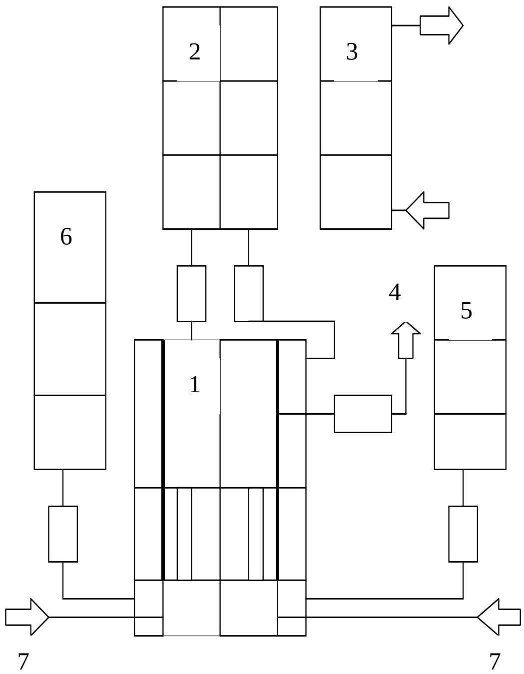

FIG.12 - TRAC MODEL
1 - RPV, 2 - SG PRIMARY, 3 - SG SECONDARY, 4 – LEAK, 5 - HP ACCUMULATOR, 6 - MP ACCUMULATOR, 7 - LP PUMPS

Natural circulation gave a flow rate of 55 kg s^{-1} at a power of 4 MW. This flow rate was sufficient to keep the minimum DNBR above 6.07, the maximum fuel temperature below 637 K and prevent bulk boiling in the core. The reactor would not be operated at higher powers in natural circulation mode as some bulk boiling would occur. Natural circulation was calculated for powers down to 0.28 MW to check that decay heat could be removed after a loss of pumped flow.

c) LOCA calculations

The largest pipe penetrating the reactor vessel was the connecting pipe to the pressuriser, which has a diameter of 50 mm. A safety injection system was proposed to keep the collapsed level of water above the top of the core in the event of guillotine fracture of the connecting pipe. The system consisted of two high-pressure accumulators of volume 1.0 m^3, pressurized to 11MPa, two medium pressure accumulators of volume 2.0 m^3 at 3.5 MPa and two low-pressure pumps supplying 9 kg s^{-1} of water at 1.0 MPa. These were added to the TRACPFQ model and several LOCA transients were modelled. For modelling purposes only one large high pressure accumulator and one large medium pressure accumulator were used.

The injection pumps were modelled by constant rate fills. The decay heat was input in tabular format shown in shorter form at Table 3. The transients calculated together with the collapsed water level reached are given in Tables 4 and 5. Each transient can be divided into three regions bounded by the pressures 11 MPa, 3.5 MPa and 1.0 MPa, as in (FIG.13). In region one only the high-pressure accumulators discharge, in region two the medium pressure accumulators discharge and in region three the pumps refill the RPV.

TABLE 3 - *Decay heat vs. time, % of power before scram*

Time (s)	0	10	100	1000	2000
Power (%)	7.0	4.5	3.0	1.8	1.0

TABLE 4 - *LOCA-collapsed water levels above top of core; full ECCS available; 50 MW before scram*

Break size	**Min level region 1**	**Min level region 2**	**Min level region 3**
50 mm	1.06 m	0.19 m	0.62 m
20 mm	1.76 m	1.80 m	1.80 m

TABLE 5 - *LOCA-collapsed levels with half ECCS available; 50 mm break; cruise speed power 15 MW; patrol speed power 4 MW.*

Initial power	**Min level region 1**	**Min level region 2**	**Min level region 3**
15 MW	0.95 m	-0.36 m	-1.05 m
4 MW	0.88 m	-0.04 m	-1.05 m

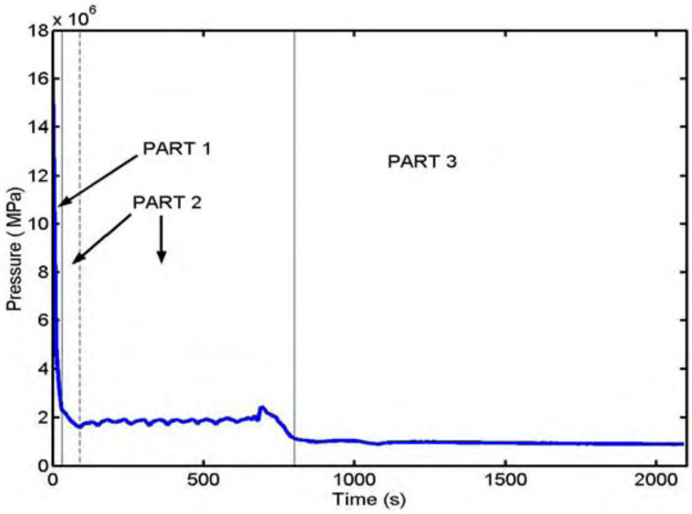

FIG.13 - PRESSURE FOLLOWING BREAK IN THE CONNECTING PIPE TO THE PRESSURISER (50 MM), INITIAL POWER 50 MW, ECCS FULLY AVAILABLE

As seen in Table 4, the core would be fully flooded when all the emergency core-cooling systems (ECCS) were available, for the largest break (50 mm) and for the highest operating power. If only half the ECCS was available the results in Table 5 indicate that part of the core became uncovered even for lower powers. The fuel rod temperatures for such a case are shown in (FIG.14). It is note-worthy that the temperature did not rise and in fact quickly fell to round 473 K. (The temperature limit assumed for the fuel was 1223 K during a transient).

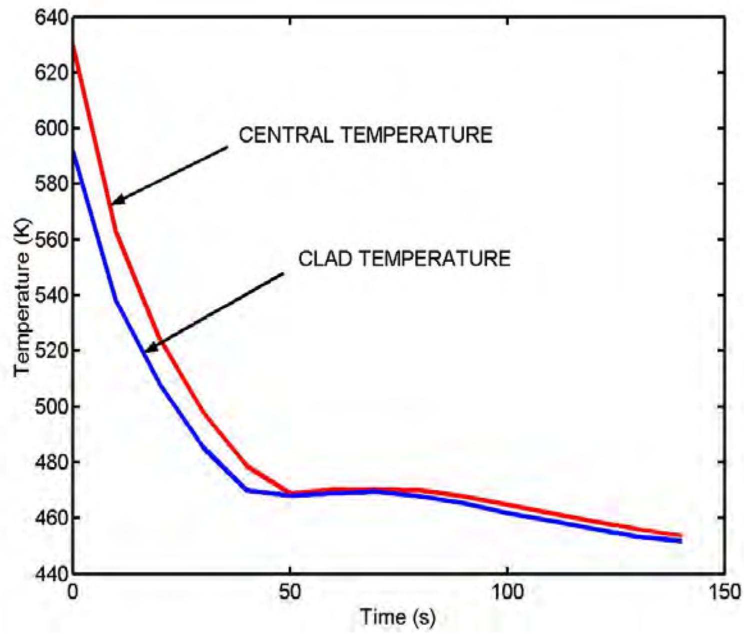

FIG.14 - FUEL AND CLAD TEMPERATURES FOLLOWING A BREAK IN THE SURGE LINE. INITIAL POWER 15 MW, HALF ECCS AVAILABLE

Reactor Dynamics

Throughout the core and plant design both the reactor physics and thermal hydraulics areas only determined the plant parameters under steady state conditions. Therefore, for the design to comply with the safety principles and criteria it was necessary to investigate the whole plant's dynamic response under normal and accident conditions. This was undertaken using Aegis Technology's acslXtreme[10] computer modelling program. This program is specifically designed to model time dependant non-linear systems such as those encountered in the development of the IMPS model.

As the intend application of IMPS was as a marine propulsion system a requirement was for the reactor plant to support a system to drive a vessel through the water. That system consisted of two steam-driven turbo-generators that are used to supply power for the electrical propulsion motor and the hotel/domestic electrical loads.

Before a whole plant model in acslXtreme™ could be developed, the entire reactor physics, thermal-hydraulics and system describing equations were required as well as all the core and system design parameters. These were then used in producing the individual sections of the plant model. At each stage of the process the model was ran in steady state conditions and the results compared against those previously obtained from TRAC and COBRA. Until finally the whole plant model was developed and tested at full power which gave comparable results from that obtain from the TRAC and COBRA codes.

Once the complete model had been produced it was decided that power changes and one severe accident condition would be examined. The accident scenario chosen was that of a guillotine fracture of the steam pipe work directly after it exits the SG which would cause an Excessive Steam Demand Accident (ESDA), the results from which would be used in the safety analysis of the whole plant.

Early in the design it was identified that the large negative temperature coefficient of reactivity of the fuel would be a major contributor to the dynamic response of the whole plant during any power change. As with a standard PWR, as the moderator temperature decreases reactivity increases, which in turn raises reactor power and the fuel temperature. However, due to the negative temperature coefficient of reactivity of the fuel, following an increase in power, the increase in fuel temperature causes the reactivity to decrease, balancing out the increase from the drop in moderator temperature. Eventually after a normal power transient the reactor settles at a higher power but with the core average temperature lower. This leads to a lowering of the steam quality exiting from the steam generator, which is unacceptable. Following a detailed study it was decided that to counteract the drop in reactivity caused by the rise in fuel temperature an automatic system would be required to move the control rods during a power transient.

With the inclusion of the automatic regulating control rod system, the model was used to analyse several normal load transients, which gave results indicating that the core would now load follow during any power change. The final part of the analysis was to determine if any core constraint would be violated during the ESDA following a guillotine fracture of the steam system. At no point during the transient did the centreline fuel temperature exceed any thermal limit. This was confirmed by the results from a TRAC analysis of the same scenario.

Safety in Design and Operation

Engineered safety in design and operation of the IMPS reactor plant were key pillars of this design study and fully integrated into the design process, ensuring that the IMPS plant, if constructed would be likely to gain regulatory approval for construction and operation.

a) Safety Principles and Safety Criteria

A set of safety principles for the project were identified based on those widely used within the UK nuclear programme[11] including: benign transient response, reduced radiological consequences from accidents over traditional designs, defence in depth and emphasis on inherent and passive safety systems. The principles of defence in depth in the prevention of accidents was particularly important in this study, leading to the addition of alternative decay heat removal system and a negative reactivity addition system to reduce the possibility of single mode failures leading to core damage.

The study also considered the requirements for introducing safety mechanisms, using the acslXtreme™ package to model operating transients. This allowed, for example, reactor SCRAM parameters to be determined, ensuring that during normal and abnormal plant operations, key plant parameters remained within the design specification and justification. The requirements to reduce PPF across the core, maintain a minimum DNBR and restricting the normal operational temperatures and pressures of the fuel to those for which a robust safety justification exists have been discussed earlier.

Whilst a full probabilistic safety assessment was beyond the scope of this project, selected relevant safety criteria were identified to demonstrate the ability of the design to meet the technical safety goals. These are shown in Table 6. The design was required to meet the basic safety limit as a minimum requirement, striving to reduce values to a level as low as reasonably practicable (ALARP) with the basis safety objective acting as a design goal, where the risks posed by the plant are considered broadly acceptable.

TABLE 6 - *Selected Design and Operational Safety Criteria for the IMPS reactor*

Safety Criterion	**Basic Safety Limit**	**Basic Safety Objective**
Doses to crew in normal operations	20 mSv yr^{-1}	2 mSv yr^{-1}
Core damage frequency (release of fission products from fuel)	10^{-3} yr^{-1}	10^{-5} yr^{-1}

b) Safety Methodology

The shielding study succeeded in proposing shielding designs meeting this criteria and an optioneering study was conducted to ensure the chosen solution was moving towards a dose burden in normal operations that was as low as reasonably practical.

A structured hazard identification process was used to identify all initiating or base events (BEs), which could lead to the top event of relevance to the second safety criterion, core damage. A fault tree was identified and indicative failure data used to give an estimate of frequency. The fault tree was used to make an initial estimate of the probability of core damage (failure of clad material) without additional protection system or reliance on the inherent safety features of the fuel. This was deemed to be unacceptable (worst bounding case initially estimated as P (top event) = 0.6/Year reduced to 0.3/Year with revised failure data). It is to be stressed however, that once this probability is modified to include the research data, showing the near perfect fission product retention of the fuel over the full range of accident scenarios, the core damage clad failure and detectable release of fission products from the safety criterion would be fully met by this design.

To reduce likelihood of clad damage (the fault tree top event) further a selection of Event trees were constructed to illustrate the large reductions in probability achievable by the introduction of relevant safety mechanisms and safety limits, thus setting the operating envelope for the plant. Specific engineering safety principles were applied to the plant and a selected deterministic analysis completed. Illustrative results showing from this demonstration of acceptability are included in Table 7.

TABLE 7 - *Specific Engineering Safety Principles used in the Design*

Design Principle	Demonstration of Acceptability of Design
Able to remove decay heat for 15 min after SCRAM from 15 MW_{th} (cooling rate 10 K h^{-1})	Requirement: 233 kW System Capacity: 300 kW Safety Margin: 28 %
Capable of cooling down the circuit with a rate of 25 K h^{-1} for 4 hours.	Achieved through heat removal of secondary steam. Minimum required capacity 814 Kg h^{-1}

Conclusions

With respect to the reactor physics study it has been demonstrated that a TRIGA™ fuelled integrated reactor plant is feasible for marine applications. A module design was developed utilising ‘commercial of the shelf’ fuel pins that the thermal hydraulic requirements. Through the use of zoned Erbium poisoning a flat power distribution within the module was achieved.

A core design consisting of 68 modules, 40 of which house a cruciform boron carbide control rod, was adopted. The inclusion of additional Erbium loaded pins and further zoning achieved a relatively flat power distribution. The worse case power peak factor was 1.43, well within the acceptable limits set by thermal hydraulics.

Burn-up calculations indicated that the desired operational lifetime of 1044 FPD could be achieved, resulting in a fuel burn-up of 44 MWd kg^{-1}. Start of life ECP and rod worth curves were successfully determined and indicated that both the required cold shutdown margin could be meet. The final core design parameters are given in Table 8.

TABLE 8 - *Final Core Parameters*

Property	Value
Fuel Height	980 mm
Effective Core Diameter	1280 mm
Number of modules	68
Fuel	$U/ZrH_{1.6}$
Burnable poison	Er
Control Rods	B_4C
Fuel loading	45% by mass
^{235}U enrichment	19.7% by mass
Nos. of fuel pins	2448
Pin radius	6.9 mm
Cladding thickness	0.4 mm
Pin pitch	16.3 mm
Nos. control rods	40
Hot Start of life k_{eff} (unrodded)	1.167
Hot Start of life k_{eff} (fully rodded)	0.8636
Cold Start of life k_{eff} (unrodded)	0.9682

It was concluded from the shielding study that the dose rates in occupied spaces when the plant was operating at full power fell within the Basic Safety Objective, whilst keeping the shielding weight burden below the permitted maximum.

The combination of TRIGA™ fuel and an integrated plant offers significant benefits from a thermal hydraulics viewpoint. Standard sized fuel pins can be spaced to keep minimum DNBR greater than 1.3 during full power operation with pumped flow. In natural circulation flow a power sufficient for patrol speed is available. In all cases the fuel temperatures are well below the safety limit. In the event of a LOCA the core can be kept flooded for the largest break size with a feasible ECCS. Even when the water level falls below the core (e.g. with only half the ECCS available), fuel and clad temperatures remain low for the calculations performed in this study.

The plant dynamics study clearly indicated the strong negative fuel coefficient of reactivity would not allow load following in the conventional sense. An automated rod control system was modelled such that the plant would respond to a load demand.

Whilst this is not a complete and exhaustive safety study, the elements chosen have clearly demonstrated that where studied the design is able to meet the safety criteria and safety principles set and that applying these principles throughout the design process has produces a system likely to gain authority to operate from a regulatory body.

Disclaimer

Any views expressed are those of the authors and do not necessarily represent those of the Nuclear Department HMS SULTAN, Flagship Training Limited or those of Her Majesty's Government. Nor should any mention of any commercial product be taken as an endorsement by the Nuclear Department, Flagship Training Limited or Her Majesty's Government.

References

1. J. Brifford, P. Pellerin, J.M. Brushwood, A.C. Thompson, P.A. Beeley, "The Feasibility of Using a 15 MW General Atomics TRIGA Core as an Autonomous Marine Power Supply", Proceedings of the 2003 International Congress on Advances in Nuclear Power Plants (ICAPP '03), Cordoba, (2003).
2. C. Iorgulis, M. Ciocanescu, M. Preda, M. Mladin, "Neutronic Calculations regarding the New LEU 6x6 Fuel Bundle for 14 MW TRIGA-SSR, in order to increase the Reactor Power up to 21 MW". 1998 International Meeting on Reduced enrichment for Research and Test Reactors, Sao Paulo, October 18-23, 1998.
3. www.sercoassurance.com/answers/, SERCO Assurance.
4. C.J. Dean, R.J. Perry, "The 1996 WIMS Nuclear Data Library", AEAT-0293, (1996).
5. M.J. Roth, "Resonance absorption in complicated geometries", AEEW R 921 (1974).
6. M.J. Halsall, "A Users Guide to the WIMS-E Module WCACTUS", AEEW R 1710 (1983).
7. G.B. West, W.L. Whittemore, J.R. Shoptaugh, J.B. Dee, C.O. Coffer, "Kinetic Behaviour of TRIGA Reactors", General Atomics GA-7882 (1967).
8. D. Basile, M. Beghi, R. Chierici, E. Salina and E. Brega, "COBRA - EN an upgraded version of the COBRA-3C/MIT code for Thermal-Hydraulic Transient Analysis of Light Water Reactor Fuel Assemblies and Cores", Report No. 1010/1 ENEL-CRTN, Synthesis S.r.l, NEA-1614.001, NEA data bank, (1999).
9. D. Basile, E. Brega, "TRAC-PF1-EN/MOD3 a TRACP-PF1 revised version inclusive of a three dimensional neutron kinetics model based on high accuracy two group nodal diffusion methods", Report No. 1037/3 Synthesis S.r.l, NEA-1593.001, NEA data bank, (1997).
10. www.acslxtreme.com , Aegis Technology.
11. Safety Assessment Principles for Nuclear Plants, Health and Safety Executive, HMSO ISBN 011 882043.
12. Department of Chemical Engineering, Columbia University, "Parametric Study of CHF Data, Volume 2: a Generalized Subchannel CHF Correlation for PWR and BWR Fuel Assemblies", Electric Power Research Institute, NP-2609, March 1982.
13. General Atomics Report UZR-28, "Uranium-Zirconium Fuels for TRIGA Reactors", General *Atomics* 1997.