Coupled neutronic/thermal-hydraulic hot channel analysis of high power density civil marine SMR cores

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Abstract

Core average power density of standard small modular reactors (SMR) are generally limited to 60–65 MW/m^3, which is 40% lower than for a standard civil PWR in order to accommodate better thermal margins. While designing a SMR core for civil marine propulsion systems, it is required to increase its power density to make more attractive for future deployment. However, there are obvious thermal-hydraulic (TH) concerns regarding a high power density (HPD) core, which needs to be satisfied in order to ensure safe operation through accurate prediction of the TH parameters.

This paper presents a coupled neutronic/thermal-hydraulic (TH) hot channel analysis of a HPD 375 MWth soluble-boron-free PWR core using 19.25% ^{235}U enriched micro-heterogeneous ThO_2-UO_2 duplex fuel and 16% ^{235}U enriched homogeneously mixed all-UO_2 fuel with a 15 effective full-power-years (EFPY) core life. To perform this analysis the hybrid Monte Carlo reactor physics code MONK is coupled with sub-channel analysis TH code COBRA-EN. This approach is used to investigate the feasibility of different HPD marine PWR concepts and to identify the main TH challenges characterising these designs. To design HPD cores of between 82 and 111 MW/m^3, three cases were chosen by optimizing the fuel pin diameter, pin pitch and pitch-to-diameter ratio. These cases have been studied to determine whether TH safety limits are satisfied by evaluating key parameters, such as minimum departure from nucleate boiling ratio, surface heat flux, critical heat flux, cladding inner surface and fuel centreline temperatures, and pressure drop. The results show that it is possible to achieve a core power density of 100 MW/m^3 for both the candidate fuels, a $\sim$50% improvement on the reference design (63 MW/m^3), while meeting the target core lifetime of 15 EFPY and remaining within TH limits. The size of the pressure vessel can therefore be reduced substantially and the economic competitiveness of the proposed civil marine PWR reactor core significantly improved.

Keywords: Civil marine propulsion, Small modular reactor, Soluble-boron-free operation, Micro-heterogeneous thorium-based duplex fuel, Neutronic/thermal-hydraulic coupling,

Hybrid Monte Carlo, Sub-channel analysis, High power density SMR

1. Introduction

The interest in atomic engines for marine propulsion was stimulated by the discovery of fission in 1938 (Hirdaris et al., 2014, Ragheb, 2011), where battery-like nuclear reactors would power different means of transport. Most of the early implementations of marine nuclear technology were inspired by the innovations in the naval marine sector. The US Navy first considered the possibility of using battery-like nuclear PWR technology in the 1940s. The motivation behind the use of PWRs was to transform their submarines from slow vessels to warships capable of sustaining speeds of about 40 km/h while staying submerged for many weeks (Khlopkin and Zotov, 1997, Hirdaris et al., 2014, Vergara and McKesson, 2002). The U.S. 'Nuclear Navy' has a record of reliable power production with no major radiation releases throughout 5400 reactor-years of operation (Hirdaris et al., 2014, Vergara and McKesson, 2002). A number of U.S. laboratories, including Bettis and Knolls Atomic Power Laboratories, are working to further develop naval nuclear propulsion technology. Furthermore, the technical success of naval nuclear propulsion created a promising pathway for commercial nuclear ships and it has benefitted from the demonstration of the effectiveness of naval nuclear technology over a period of more than 60 years. Although the development of civil marine nuclear ships began in 1950s, it was not commercially successful due to the suffering from the costs of specialized infrastructure. Four merchant nuclear vessels: NS Savannah, NS Otto Hahn, NS Mutsu and NS Sevmorput have been commissioned by USA, Germany, Japan and Russia, respectively since 1950 (Oelgaard, 1993, Hirdaris et al., 2014).

U.S. naval reactors use very highly enriched uranium fuel in order to ensure longer core lifetimes. Russia exhibited long experience (∼60 years) in nuclear-powered icebreakers. Historically Russian ships (e.g. OK-900, KLT-40) have employed with more than 20% ^{235}U enrichment (Bukharin, 2006), but a new generation of icebreaker cores (e.g. KLT-40S, RITM-200) are reported to use less than 20% ^{235}U (Zverev et al., 2013) enrichment.

There are several interconnected reasons why nuclear propulsion has never played a significant role in the civil maritime sector:

1. There are political barriers posed by popular antinuclear sentiment and the reluctance of shipyards and ports to accommodate nuclear vessels (Dedes et al., 2011, Kramer, 1962).
2. There is no infrastructure for nuclear maritime refueling, maintenance or security in place.
3. Potential operators are deterred by the legal and regulatory uncertainty surrounding nuclear propulsion, which is further complicated by the issue's international scope (Namikawa et al., 2011).

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4. Nuclear reactors are a costly and potentially risky option, requiring about twice the capital investment of comparable diesel engines (Aspelund et al., 2006).
5. Nuclear ships also face a range of technical and engineering challenges, including:
   (a) Non-proliferation concerns, necessitating the use of uranium with less than 20% enrichment;
   (b) The need for flexibility and high availability, which requires the reactors to have long refueling intervals and to be capable of easily varying power;
   (c) Safety and stability standards, requiring a high level of passive safety, security, and engineering simplicity for maritime operation with limited support capability.

Engineering solutions to these problems are further constrained by the demands of the onboard environment, which include pitching and rolling, space/weight limitations, and safety/shielding concerns (Carlton et al., 2011).

With increasing attention being given to greenhouse gas emissions arising from the burning of fossil fuels for international air and marine transport and the excellent safety record of nuclear-powered ships, renewed interest in marine nuclear propulsion is likely (Mitenkov et al., 2003). Looking at medium- to long-term options (Hirdaris et al., 2014), given that hydrogen is not yet ready for shipboard installation (Aspelund et al., 2006), there is currently no solution that eliminates all emissions and no other solution that can offer significant CO_2 reduction (Anger, 2010).

Reactor cores for civil marine applications would need to be fundamentally different from land-based power generation systems, which require regular refueling, and from reactors used in military vessels, as the fuel used could not conceivably be as highly enriched. For marine propulsion reactors, where size is at a premium, core average power density is an important figure of merit and characterizes design performance. Because of space and shielding constraints, it is necessary for marine reactors to be compact, with high power density (HPD). Increasing the core power density is one way to increase power production, and thus obtain better economic performance. There are, of course, many other factors that will need to be considered if nuclear power is considered for merchant vessels but many of these issues are a moving target. For instance, the cost of Uranium varies over time. Strictly speaking, the costliest portion of the process might very well be the licensing process. In recent history, the NRC has taken many years to approve new designs and the cost of that review will be significant.

The first author's PhD research (Alam, 2018) builds on Masters and PhD projects by researchers from the University of Cambridge (Sun, 2014, Otto, 2013, Fan, 2012, Zhang, 2013) and the University of Manchester (Peakman, 2014). These works focus on the low power density core (60-65 MW/m^3) with maximum of 10 years core life while employing UO_2 and Th/UO_2 fuel. Furthermore, none of these works consider thermal-hydraulics feasibility of the SMR marine cores. On the contrary, in our study, the main difference with the reference works are the investigation of new fuel micro-heterogeneous concept with 15 years core life (50% core life improvement) with improved power density. Previously no study has been conducted for thorium-based micro-heterogeneous fuel concept for soluble-boron-free (SBF), small modular reactor (SMR) application. A limited number of studies of micro-heterogeneous duplex fuel are available in the public domain, but its use has never been examined in the context of a SBF environment for long-life core (Zhao, 2001, MacDonald and Lee, 2004). The principal objective of this PhD research was to design a SBF, SMR core with high power density (>100 MW/m^3) using low enrichment uranium (LEU) fuel (<20% ^{235}U) that provides at least 15 effective full-power-years (EFPY) life at 333 MWth. One of the main motivations was to explore an alternative candidate fuel platform that will enable the SBF core to achieve maximum attainable life for the HPD cores while satisfying all the neutronics/thermal-hydraulic safety parameters.

Previous studies of marine propulsion systems have been limited to cores with low average power density (<70 MW/m^3) (Peakman, 2014, Sun, 2014, Otto, 2013, Fan, 2012, Zhang, 2013, Ippolito, 1990). In our preliminary sizing calculations, it was found that the power density was some 40% *lower* than for a standard civil PWR (Alam, 2018). This was mainly a consequence of the high fuel mass (17.4 tonnes) required for long core life. As discussed, a marine reactor can be made more attractive for future deployment by increasing its power density. The most important fuel rod design parameter for a HPD core is the pitch-to-diameter ratio (P/D). A higher power density and more compact core size can be realised as the pin pitch is reduced. However, there are obvious thermal-hydraulic (TH) concerns regarding a HPD core. At steady state, due to the increased pressure drop, a tight lattice leads to a reduced coolant flow rate. This, in turn, leads to a higher temperature rise across the core, which affects temperature limits and the minimum departure from nucleate boiling ratio (MDNBR). Therefore, the design of HPD cores requires accurate prediction of the TH parameters in order to ensure safe operation.

According to the literature, SMRs (Reyes Jr and Lorenzini, 2010, Petrovic et al., 2012, Carelli et al., 2004) have core power density of 60–65 MW/m^3. Since this study considers the SMRs for marine propulsion application, it is always attractive to increase the power density by reducing the core volume. In addition, commercial reactors run at power densities higher than the SMR power densities. It is not a limitation for the commercial reactors to achieve high power density since power reactors generally have significantly high power output (e.g. standard 4-loop PWR with 3,411 MW power) than that of the SMRs, which can make the overall core power density significantly higher than that of the SMR. It is also worth addressing that SMR marine core with similar geometry and design of Civilian PWRs couldn't be designed to run at a similar power density since the SMR core power is limited to 100 MWe to 300 MWe. Since power output is restricted to a range of specific values (100 MWe to 300 MWe), only the reduction of core volumes can provide the improved power density, which required reduction of pin pitch and fuel diameter. Since core power is limited according to the definition of SMR and marine propulsion cores generally exhibits lower power (∼100 MWe), it was required to increase the power density (by reducing core volume) to make the overall marine core compact and economically attractive.

Although space of the reactor compartment didn't seem to be a concern for the previous nuclear-powered ships (e.g. Savannah, Otto Hahn) (Oelgaard, 1993, Hirdaris et al., 2014), it is important to address that our study attempted to follow the core design of KLT-40S (Thermal capacity 300 MWth and average core power density 119.3 MW/m^3) marine propulsion nuclear icebreakers, which exhibits the similar profiles as our designed HPD cores. We also followed the design criteria of KLT-40S, which demonstrates higher power density under thermal spectrum and PWR configuration.

In previous work (Alam, 2018, Alam et al., 2018c,d) we have performed and presented neutronic design studies of long-life HPD candidate cores comparable in power density to Sizewell B (101.6 MW/m^3). This paper presents a coupled neutronic/TH hot channel (HC) analysis of a HPD 375 MWth SBF 15 EFPY life PWR core using 19.25% ^{235}U enriched micro-heterogeneous ThO_2-UO_2 duplex fuel and 16% ^{235}U enriched homogeneously mixed all-UO_2 fuel. The core thermal power is increased by ∼13% (from the reference 333 MWth to 375 MWth) in order to realise a HPD environment. To perform this analysis the hybrid Monte Carlo (MC) reactor physics code MONK (Long et al., 2015) is coupled with sub-channel analysis TH code COBRA-EN (Basile, 1999). The hybrid MC option offers efficiency gains over conventional MC approaches and accuracy benefits compared with traditional deterministic methods (Hutton and Smith, 2001). Coupled neutronic/TH analysis is undertaken to evaluate key TH parameters such as: MDNBR, surface heat flux (SHF), critical heat flux (CHF), cladding inner surface and fuel centreline temperatures, and pressure drop. These neutronic/TH evaluations go beyond those presented in previous studies of civil marine SMR cores (Thomet, 1999, Daing and Kim, 2011, Kim et al., 1998, Peakman, 2014, Sun, 2014, Otto, 2013, Fan, 2012, Zhang, 2013, Ippolito, 1990).

2. Design goals and constraints

In this coupled neutronic/TH HC analysis, the candidate cores use 19.25% ^{235}U enriched micro-heterogeneous ThO_2-UO_2 duplex fuel[1] and 16% ^{235}U enriched homogeneously mixed all-UO_2 fuel (Alam, 2018). Our neutronic study (Alam, 2018) showed that in order to obtain a core life of ∼15 years, higher enrichment levels are required for the candidate fuels. It is more than what is currently used in commercial reactors but it is similar to what is used in research and test reactors. Therefore, if civilian nuclear propulsion is to be used, it is possible to utilize these fuels lines subject to the recertification of the current fuel lines. Most importantly, it is obvious that currently practised 5% enrichment wouldn't be able to provide a 15 years core life and therefore, the licensing of long-life marine SMR cores will have to follow a path different from a commercial reactor. In regard to the enrichment, it can be said that new generation of icebreaker cores KLT-40S uses 14.1% U-235 enrichment and KLT-40M uses 90% enrichment (Zverev et al., 2013). This fact justifies that marine propulsion cores generally employ higher enrichment level than the practice in order to obtain longer core life.

The main goals of this TH analysis are to confirm that no design constraints are violated and to realise improvements by making use of design margins and parameter optimization. The principal design constraints are:

- The MDNBR should exceed 1.30 (Akimoto et al., 2016, Yuan, 2004, Todreas and Kazimi, 2012);

[1]We use the term 'duplex' to refer to the micro-heterogeneous ThO_2-UO_2 duplex fuel throughout this paper.

- The maximum SHF should be less than 1.57 MW/m^2, in order to increase the margin of the MDNBR (Arshi et al., 2010);
- The maximum average fuel temperature should be less than 1600 K (Todreas and Kazimi, 2012);
- The maximum fuel centreline temperature should be less than 2800 K, in order to avoid fuel melting (the actual melting point of UO_2 is 3138 K) (Todreas and Kazimi, 2012);
- The maximum cladding inner surface temperature should be less than 1000 K, in order to maintain cladding integrity and limit corrosion growth, assuming an appropriate transient margin (Todreas and Kazimi, 2012);
- The maximum pressure drop should be less than 50 kPa (Greenspan, 2005). In steady-state TH analysis, the recommended pressure drop limit for small PWR core pumping capacity is ∼50 kPa. A lower core pressure drop allows more coolant to flow through the core, and therefore it is easier to cool the fuel during a loss-of-coolant accident (LOCA).

3. Improving core power density

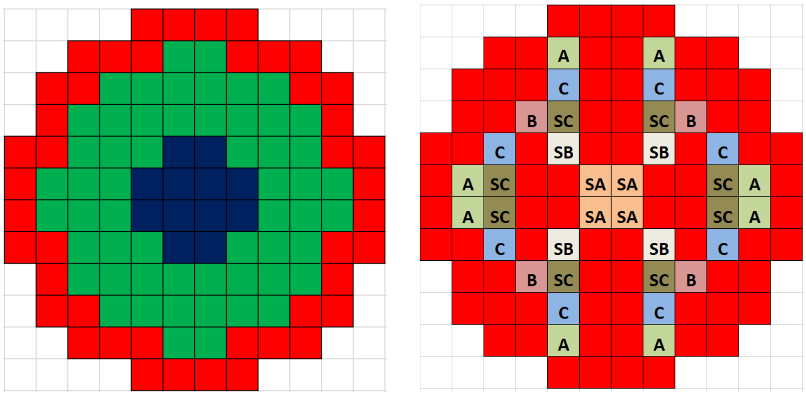
Higher power density and a more compact core can be realised principally by reducing the pin pitch (P). Fuel assembly design studies then determine the rod diameter (D) and P/D ratio that satisfy the design criteria and constraints. For square assemblies, the power density

$$Q''' = \frac{W_{\text{thermal}}}{V_{\text{core}}} = \frac{W_{\text{thermal}}}{N_{\text{assembly}} H_{\text{core}} A_{\text{assembly}}} \tag{1}$$

where W_{thermal}, V_{core}, N_{assembly}, H_{core} and A_{assembly} are the core thermal power, core volume, number of assemblies, core height and assembly area, respectively. Core height is changed as core diameter D_{core} changes according to the optimal height-to-diameter ratio for a minimum volume critical cylindrical reactor: $H_{\text{core}} = 0.93 D_{\text{core}}$. It is obvious from Eq. (1) that reducing A_{assembly} leads to a higher Q'''. A_{assembly} can be decreased by reducing the pin pitch P. In this study, the area of the square assembly was optimized by changing the fuel rod diameter D and the lattice pitch-to-diameter ratio P/D. The design space explored is $0.80 \leq D \leq 0.95$ cm and $1.22 \leq P/D \leq 1.25$. In addition, W_{thermal} is increased from the reference 333 MW to 375 MW in order to realise a HPD environment. In order to compensate for the loss of fuel loading in the HPD cases, slightly higher fissile enrichment (^{235}U content: 16% for all-UO_2 and 19.25% for duplex fuel) was required in all 112 assemblies in the core to obtain a comparable lifetime to that of the reference core (^{235}U content: 15% for all-UO_2 and 18% for duplex fuel).

From the point of view of TH and mechanical safety, there is a lower limit on the 'minimum gap clearance' $G_C = P - D$. It is recommended that $G_C > 1$ mm for HPD PWR cores (Todreas and Kazimi, 2012). Narrow pins gaps could push the core flow velocities into regions of mass flux $> 5{,}500$ kg/m^2/s which would induce vibrations (Todreas and Kazimi, 2012). To avoid this problem, the HPD cases were designed in such a way that mass flux < 5,000 kg/m^2/s. Table 1 shows the cases examined in this study with the associated TH parameters (Alam, 2018, Alam et al., 2018c,d).

Furthermore, Table 2 shows the core dimensional gain (%) in the HPD cases compared to the reference core. Cases 1, 2 and 3 obtain ∼8%, ∼17% and ∼20% reduction in core diameter, respectively; and ∼15%, ∼30% and ∼37% reduction in core volume, respectively.



(a) **(b)**

Fig. 1. (a) Radial zoning pattern in the 112-assembly core: blue = A, green = B, red = C; (b) Position of control rod banks.

It is also important to address how core life was estimated for the HPD cases, as shown in Table 1. Whole-core calculations have been performed using PANTHER (Hutt, 1992) nodal diffusion code. Using PANTHER, we modeled the 112-assembly core, divided into three radial zones (A, B and C, see Fig. 1a). In order to suppress reactivity, we used both burnable poison and control rods. The number of IFBA pins distributions in the assemblies in Zones A, B and C for the HPD cases are given in Table 3. Using 100 μm IFBA pins, core life was calculated where through-life reactivity swing is suppressed below 4000 pcm. Finally, criticality has been achieved by using B_4C control rod banks while achieving satisfactory neutronic safety parameters (Alam, 2018). In our 112-assembly marine PWR core, 3 banks of control rods (A, B and C) are used for power maneuvering and 3 other banks (SA, SB and SC) are used for shutdown (as shown in Fig. 1b). For the HPD cases, a total of 37 rod cluster control assemblies (RCCAs) each of 16 rods are used (Alam, 2018).

4. Methodology

4.1. Rationale for using coupled methods and hot channel evaluation

Coupled neutronics/TH (multiphysics) codes have a proven record in TH evaluation. All commercial reactor core analysis codes have neutronics and TH solvers. Multiphysics codes are needed in the modeling of power reactors because the input and output of neutronics calculations depends on TH parameters and vice versa (Oliveira, 2016, Waata, 2006, Torres

Parameters	**Reference**	**Case 1**	**Case 2**	**Case 3**
Thermal power (MWth)	333	375	375	375
System pressure (MPa)	15.50	15.50	15.50	15.50
Assembly size	13×13	13×13	13×13	13×13
Pin pitch P (mm)	12.60	11.60	10.50	10.00
Pin outer diameter D (mm)	9.50	9.50	8.47	8.00
Fuel outer diameter (mm)	8.19	8.19	7.16	6.70
Pitch-to-diameter ratio P/D	1.33	1.22	1.24	1.25
Gap clearance G_C (mm)	3.10	2.10	2.03	2.00
Number of assemblies	112	112	112	112
Core diameter (m^2)	1.96	1.80	1.63	1.55
Core volume (m^3)	5.38	4.56	3.74	3.39
Wetted perimeter (mm)	29.83	29.83	26.59	25.12
Hydraulic diameter (mm)	11.79	8.54	8.12	7.92
Mass flow (kg/s)	5716	5716	5716	5716
Mass flux (kg/m^2/s)	2346	3237	3822	4145
Fuel area (mm^2)	211.13	211.13	161.42	140.95
Water area (mm^2)	87.91	63.71	53.93	49.76
Metal/Water	2.40	3.31	2.99	2.83
Fuel mass reduction (%)	0.00	0.00	23	33
Relative fuel mass (%)	100	100	77	67
^{235}U content[a] (%)	15/18	16/19.25	16/19.25	16/19.25
Core life[a] (EFPY)	15.25/16	15/15.53	14.40/15	13.83/14.68
Core radial form factor[b]	1.43/1.37	1.44/1.48	1.38/1.42	1.37/1.38
Assembly peaking factor[a]	1.34/1.33	1.33/1.32	1.32/1.32	1.25/1.25
Core total form factor[a]	2.14/2.15	2.08/2.17	1.93/2.05	1.86/1.95
Inlet temperature (K)[a]	588.46/587.58	589.77/589.79	590.17/590.09	590.88/590.65
Outlet temperature (K)[a]	605.12/603.69	607.29/607.15	608.05/607.91	609.1/608.95
Power density (MW/m^3)	62	82	100	111

[a] Values are shown for UO_2/duplex fuels respectively.
[b] Hottest assembly from whole-core map. Values are shown for UO_2/duplex fuels respectively.

Table 1. Design parameters of the reference and HPD marine cores (Alam, 2018, Alam et al., 2018c,d).

Dimension	**Case 1**	**Case 2**	**Case 3**
Core diameter gain (%)	7.94	16.67	20.63
Core volume gain (%)	15.24	30.56	37.01

Table 2. Gain (%) in core dimension compared to the reference core.

Zones	Case 1		Case 2		Case 3	
	Duplex	UO_2	Duplex	UO_2	Duplex	UO_2
A	49	53	45	49	45	49
B	45	49	41	41	37	37
C	25	25	13	17	13	13

Table 3. 100 µm IFBA pins distributions for the HPD cases.

et al., 2011). This is in contrast to, for example, the modeling of zero-power research reactors, where temperatures and densities are approximately constant.

PANTHER is an advanced 3D nodal code that models reactor cores at the assembly level, while sacrificing detail at the pin level. In the approach used in this study, a full-core calculation is made using nodal methods, the hot channel is identified and this channel is modeled in detail with coupled MC and sub-channel analysis codes. This approach thus models the hottest assembly at the pin level but would be computationally very expensive to apply to a full core.

This high-fidelity analysis is motivated by considerations of reactor safety, licensing and economics. In order to license a reactor design, it is necessary to prove its safety by demonstrating that specific criteria are satisfied with some margin. If low-fidelity analysis codes are used, more conservative choices are necessary. High-fidelity methods reduce the need for conservatism and allow the design to be closer to the safety limits. This implies that it is possible to make a safety case for increasing the power output (uprating) without changing the design of the reactor.

The method for finding the hot channel is quite standard (Todreas and Kazimi, 2012, Torres et al., 2011, Alam et al., 2016). For each case considered (Table 1), the through-life hot channel was identified by finding the pin with the highest power. The highest assembly power was found by multiplying the average assembly power and the assembly power peaking factor (for the hottest assembly location from the PANTHER whole-core map). The highest pin power (hot channel) in the hottest assembly was found by multiplying the average pin power for that assembly and the assembly-level pin power peaking factor. Since the hottest pin has the highest power and all the pins have the same surface area, this is also the pin with the highest heat flux and thus the pin closest to the critical heat flux. If this pin satisfies the specified safety criteria, then the other pins will as well. Modeling only the hottest pin is a conservative approach. Since there is no cross-flow in a single pin case, this is effectively equivalent to modeling a whole core consisting only of hot pins (Arshi et al., 2010, Oliveira, 2016).

4.2. Coupling method and codes used

MONK (Long et al., 2015) is a validated reactor physics code used extensively for reactor safety and criticality applications, especially in the UK. It is a time-independent MC neutronics code that uses discrete energy groups (hybrid MC) when executed as part of the WIMS package (Newton et al., 2008). Our study uses nuclear data derived from the JEF2.2 database available from the IAEA. During MONK execution, the collision probabilities module PERSEUS is used in conjunction with PIP to generate fluxes for energy group condensation. Then cross-sections and fluxes are condensed to 6 energy groups before proceeding with the MONK calculation.

COBRA-EN (Basile, 1999) is a sub-channel code system used for both steady-state and transient TH analyses of LWR fuel assemblies and cores. It uses the typical three-equation model (mass, energy and momentum) of the two-phase mixture, extended with a vapour continuity equation and improved pellet heat conduction model based on the one in VIPRE (Stewart et al., 1989). A coolant sub-channel centred scheme is used in this study. Sub-channel analysis allows the user to analyse an array of single fuel pins, which partition the coolant flow area into small sub-channels. For the pressure drop (ΔP) calculation, ΔP from core inlet to outlet was forced to be uniform by adjusting the inlet mass flows, and the water properties were computed as a function of the local pressure instead of the exit reference pressure (Basile, 1999). We have considered 3 spacer grids for this analysis: a structural one at the bottom and 2 mixing ones at 1/3 and 2/3 of the rod length, respectively. Average fuel and cladding surface temperatures were extracted for 1 coolant channel, 21 axial intervals and 5 radial nodes for this study.

A boiling curve consisting of three heat transfer regimes was considered. These regimes are: single-phase, sub-cooled, and saturated nucleate boiling. We have used various correlations (Arshi et al., 2010, Silva et al., 2014):

- The Dittus-Boelter correlation (Basile, 1999) for the heat transfer coefficient in single-phase flow;
- The Thom + single-phase liquid correlation for the heat transfer coefficient in the sub-cooled and saturated nucleate boiling region;
- The EPRI correlations used to determine the CHF point on the boiling curve can be written as (Basile, 1999, Arshi et al., 2010):

$$q''_{\mathrm{CHF}} = \frac{1}{0.0036} \frac{AF_A - x_{in}}{CF_c F_g F_{nu} + (\frac{h-h_{in}}{0.0036.q''.h_{fg}})}$$

where

$$A = 0.5328.P_r^{0.1212}.(0.0036.G)^{(-0.3040-0.3285.P_r)}$$

$$C = 1.6151.P_r^{1.4066}.(0.0036.G)^{(-0.4843-2.0749.P_r)}$$

Here, P_r is the critical pressure ratio (= system reference pressure/critical pressure), x_{in} is the inlet vapour quality, h is the local equilibrium specific enthalpy, h_{in} is the inlet specific enthalpy, h_{fg} is the specific enthalpy of vaporisation, G is the coolant mass flux, and F_A, F_C, F_g and F_{nu} are optional factors which correct the value of q''_{CHF} for various effects; otherwise they are assigned default values of 1.0. The approximate applicability ranges of pressure, mass flux, heated length and hydraulic diameter for all correlations have been considered (Silva et al., 2014). It is important to address that with the rising concern for improving passive safety; several integral and separate effect tests are on-going with modified fuel length to improve, verify and validate the TH models, and correlations. In this study, the most cited correlations and models applied with the most conservative design approach.

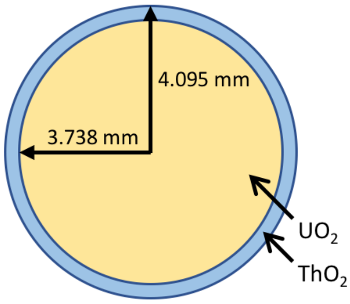


Fig. 2. Configuration of the reference micro-heterogeneous duplex ThO_2-UO_2 fuel.

COBRA-EN cannot model heat conduction in micro-heterogeneous duplex fuel (as shown in Fig. 2), only in homogeneous fuel. UO_2 was therefore used as an approximation for duplex fuel in the sub-channel code. This approximation is considered acceptable for two reasons. First, the conductivity of UO_2 is worse than that of ThO_2 (Bobkov et al., 2008), so the COBRA-EN model provides conservative results for the temperatures of duplex fuel. Second, as shown in Fig. 2, the layer of ThO_2 is thin ($\sim$20% of the UO_2 region) and is therefore expected to have only a minor influence on the predicted temperatures.

In order to calculate the temperature in UO_2 and ThO_2 nodes, the volume-averaged values of the radii of each node are used. In COBRA-EN, the fuel pellet is discretized into equal thickness rings. The volume-average radii of these rings and temperatures in each ring is used to fit a third degree polynomial to describe the temperature profile. This function is used to reconstruct the expected temperature of each ring in the neutronics solver, which uses an equal volume discretization approach. The values predicted are passed to MONK for the neutronics modeling.

4.3. Coupling scheme and data exchange

The coupling is achieved through a modular program developed in Python. It is designed in such a way that MONK or COBRA-EN can be replaced by any other appropriate codes and invoked upon a simple change of input file (Oliveira, 2016). A Picard Iteration scheme is used, as illustrated in Fig. 3.

The channel power is used to calculate a flat power profile (i.e. every axial node at the same power). Calculations begin by executing COBRA-EN with the flat power profile to generate initial density and temperature profiles. The axial and radial power peaking factors of each axial mesh were extracted from the PANTHER output for the cases considered (Alam, 2018, Alam et al., 2018c,d). These are used in MONK to generate a new power profile and the sequence is repeated. Relaxation methods are not yet implemented, so the iteration

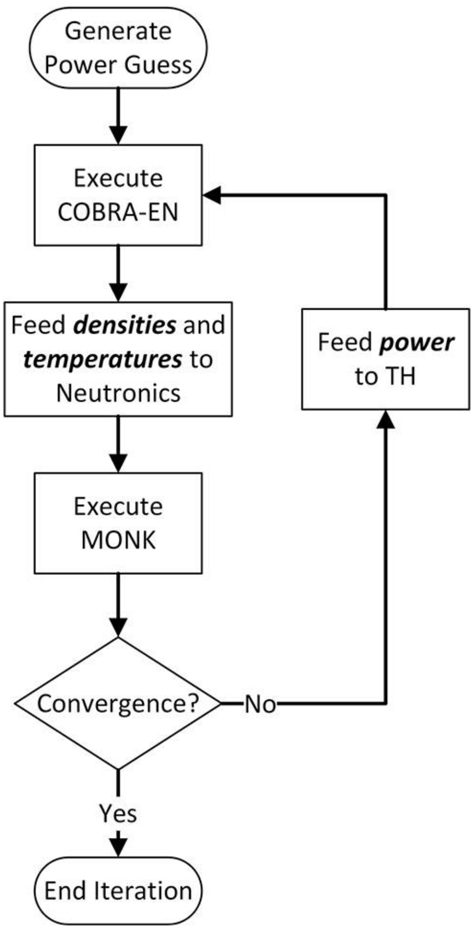


Fig. 3. Neutronics/thermal-hydraulics iteration scheme.

scheme was repeated a fixed number of times (10). This number of iterations was found to give good convergence.

Data exchange is managed through a loosely coupled method where each code retains its own input and output files, which are generated or interpreted by the Python interface module. The coupling script also translates the sub-channel centred geometry used in COBRA-EN to the rod-centred geometry used by MONK. It does so by averaging water properties of the four channels around a rod to derive rod-centred properties.

Fig. 4 shows how the system of codes is interconnected. All physics modules are linked with the multiphysics interface and the intention is for them to be freely interchangeable. This interface is responsible for iteration and exchange of information between neutronics and thermal-hydraulics modules, for keeping track of the history of the iterative process and for checking for convergence of the system. Two-way coupling is performed between neutronics and thermal-ydraulics. Since MONK is executed as part of the WIMS package, the neutronics segment of the system architecture is referred to using the title WIMS (e.g. WIMS Generator).

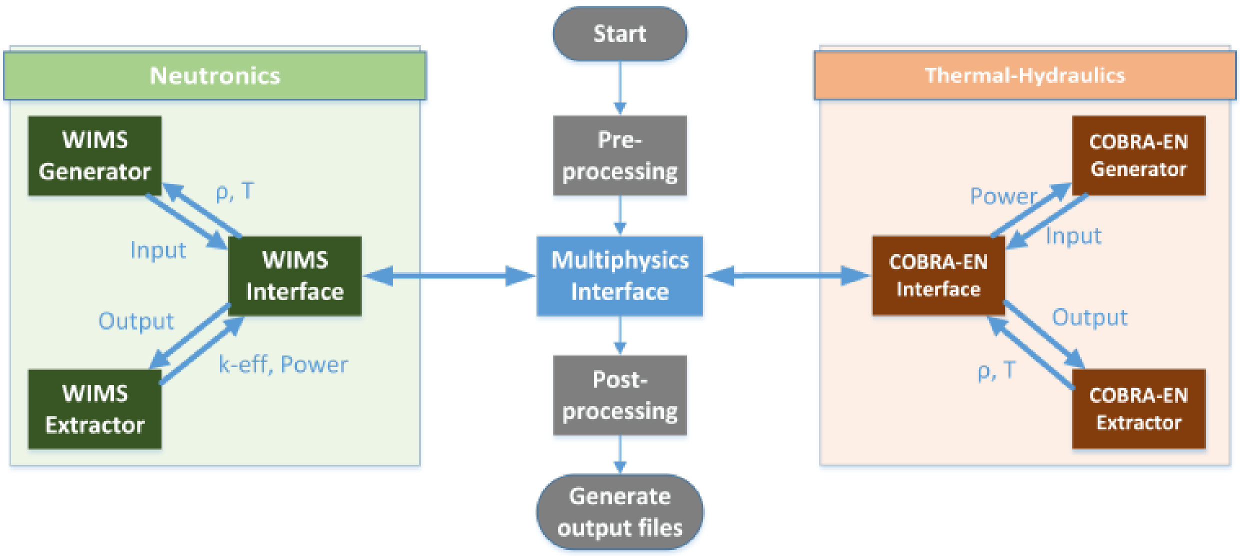


Fig. 4. Overview of the multiphysics software architecture.

Pellet discretization was implemented in the neutronics module so as to be consistent with the implementation in the COBRA-EN module. The function receives the temperatures and radius of the corresponding region and discretizes the pellet in accordance with the data sent from the TH module. Determining how a pellet will be divided and calculating temperatures in these divisions is a function of the TH module, which is aware of the capabilities and limitations of the underlying program.

5. Hot channel analysis of improved power density cores

5.1. Heat flux and DNBR

For the steady-state HC calculations, a maximum overpower of 118% was considered to accommodate reactor transients. HC analysis was performed for the three HPD cases to investigate MDNBR, maximum fuel centreline and cladding temperatures, and pressure drop at a constant mass flow rate of 5716 kg/s (0.302 kg/s per pin channel, equivalent to a Westinghouse 4-loop PWR) (Todreas and Kazimi, 2012). Fig. 5a shows that MDNBR values for all cases are well above 1.30. The HPD lattice geometry results in a smaller coolant flow area and therefore, if the core power is kept constant, a higher heat flux to the coolant, which has a detrimental effect on MDNBR. Cases 1 and 3 experience the highest and lowest MDNBR, respectively, in line with their coolant flow areas (Fig. 5b). In addition, it is observed that the reference cases for the candidate fuels experience higher MDNBR values than the HPD cases, due to their higher coolant flow areas.

Throughout this study, the differences (%) in TH parameter values, generically *par*, for the two candidate fuels at different axial levels for the different HPD cases have been calculated as $[(par(\mathrm{UO_2}) - par(\mathrm{duplex}))/par(\mathrm{duplex})] \times 100\%$.

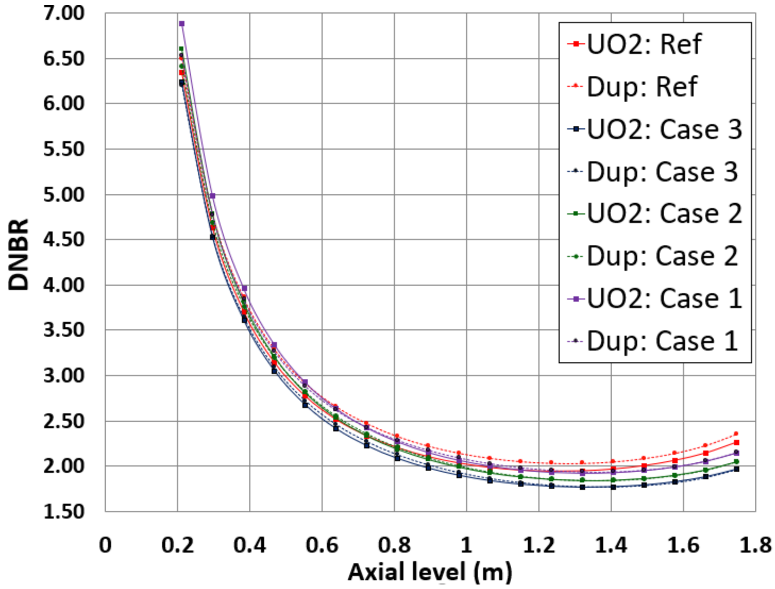
Figs. 5a and 6 show that, for Cases 1 and 2, the DNBR values are higher for the all-UO_2 fuel than for the duplex fuel at the lower axial levels (0–0.80 m) of the fuel rod due to the higher SHF (Fig. 7a) experienced by the duplex fuel. Things are different in the upper axial levels (0.80–1.79 m) of the fuel rod where UO_2 SHF values are larger and, therefore, DNBR values are marginally higher for duplex fuel. For Case 3, the highest power density case, DNBR values are higher for the UO_2 fuel at the ends of the rod and higher for the duplex fuel in the middle.

It is recommended that the maximum SHF should be less than 1.57 MW/m^2 in order to maintain a safer design by reducing the stress and thermal wear on the cladding and support material (Arshi et al., 2010). Fig. 7a shows that the SHF values in the hot channel for Cases 1 and 2 are below this limit, unlike Case 3. However, Case 3 does not violate the MDNBR limit for either fuel. As expected, the reference cases for both candidate fuels exhibit lower SHF values than for the HPD cases.

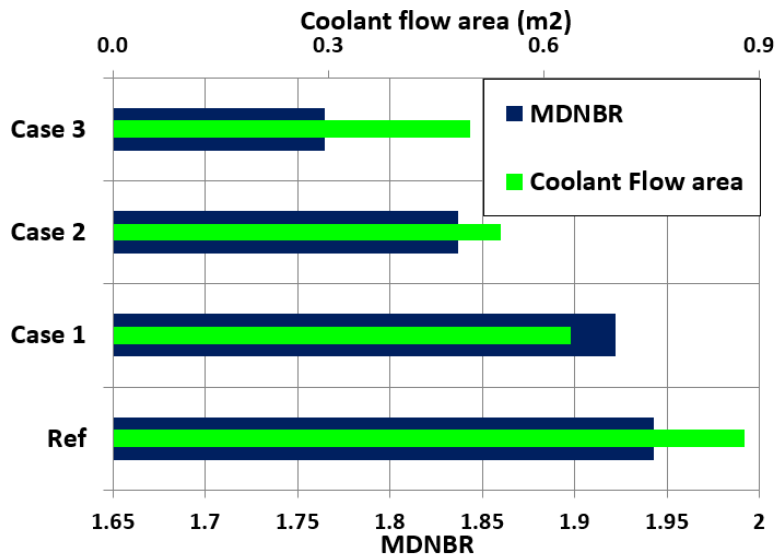
Table 1 shows, for the HPD cases, that the lowest mass flux (3237 kg/m^2/s) occurs in Case 1 (82 MW/m^3) and the highest (4145 kg/m^2/s) in Case 3 (111 MW/m^3); these cases have the highest and lowest MDNBR, respectively. This can be explained by the relation between the maximum SHF and CHF values along the fuel rod and the coolant mass flux. In order to compare the rate of increase of SHF and CHF, relative values are considered. Fig. 7b shows that the rate of increase of CHF is smaller than that of SHF with increasing mass flux. Therefore, MDNBR margin is degraded with increasing mass flux and core power density. Overall, it can be concluded that MDNBR will not be a limiting factor for these HPD designs for either candidate fuel.

5.2. Fuel temperature

It is required that there be no melting of the fuel and cladding. Since oxide-based fuels have been specified, it is important to recognise that they release non-negligible amounts of



(a)



(b)

Fig. 5. (a) DNBR for different HPD cases; (b) Hot channel MDNBR and coolant flow area for UO_2 fuel for different HPD cases.

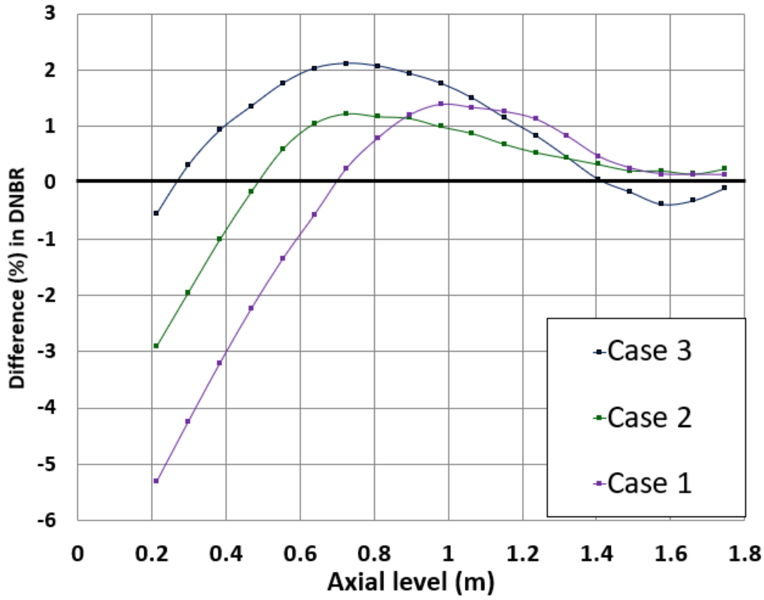


Fig. 6. Differences (%) in DNBR between UO_2 and duplex fuel for different HPD cases.

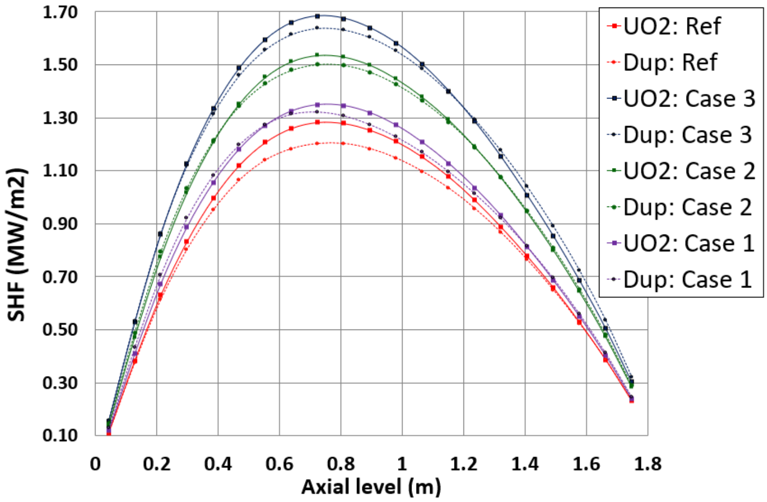
fission gas (Todreas and Kazimi, 2012). If this gas release is not managed or restricted, it can pressurize and even burst the fuel pin. Generally, fission gas release for a PWR should be less than 5%, and can be kept lower by limiting the average fuel temperature to 1600 K and cladding surface temperature to 1000 K (for Zircaloy) in steady-state operation. The average fuel temperature constraint is considered to be more limiting than imposing a peak fuel centreline temperature of 2800 K (Todreas and Kazimi, 2012).

Fig. 8a shows that fuel centreline temperatures for both fuels are safely below their limiting values for all three HPD cases. In contrast, Fig. 8b shows that only Cases 1 and 2 satisfy the cladding inner surface temperature limit of 1000 K; Case 3 exceeds the permissible limit by ~30 K for both fuels. Fig. 8c shows that, although Cases 1 and 2 satisfy the average fuel temperature limit of 1600 K, Case 3 exceeds it by ~60 K for both fuels. As expected, the corresponding temperatures for the reference cases are lower than for all HPD cases.

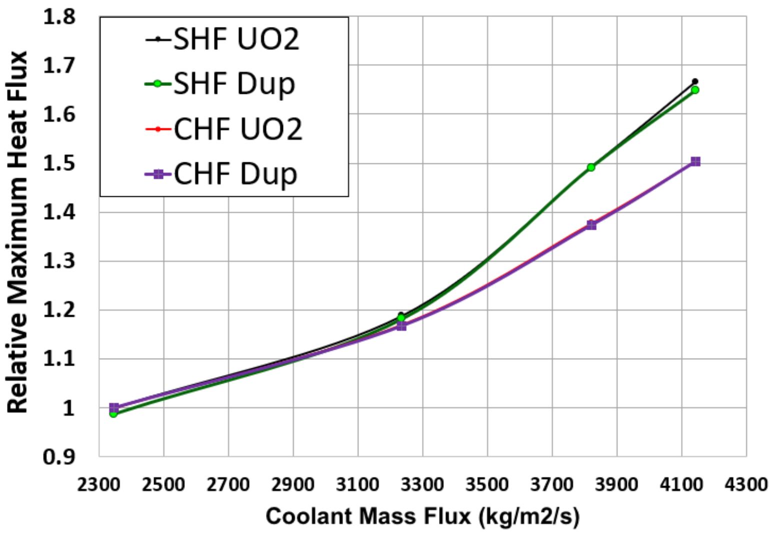
As shown in Fig. 8, all the temperature profiles are, as expected, skewed towards the bottom of the core. The lower coolant temperature (and higher density) in the bottom part of the core leads to more efficient moderation, enabling more neutrons to reach thermal energies where fission cross-sections are higher. This effect pulls the peak power from the centre of the core down to an axial level of around 0.70 m.

The fact that the cladding surface temperature is below its limit for Cases 1 and 2 improves safety, as this allows for a larger temperature rise before failure in an accident. The fuel and cladding surface temperatures for higher power density cases (e.g. Case 3) are higher than for lower power density cores (e.g. Case 1), as expected due to their comparatively lower coolant flow areas (Fig. 9).

Figs. 10a and 10b show that the fuel centreline and cladding inner surface temperatures are higher in the duplex fuel than the UO_2 fuel towards the ends of the fuel rod, whereas these

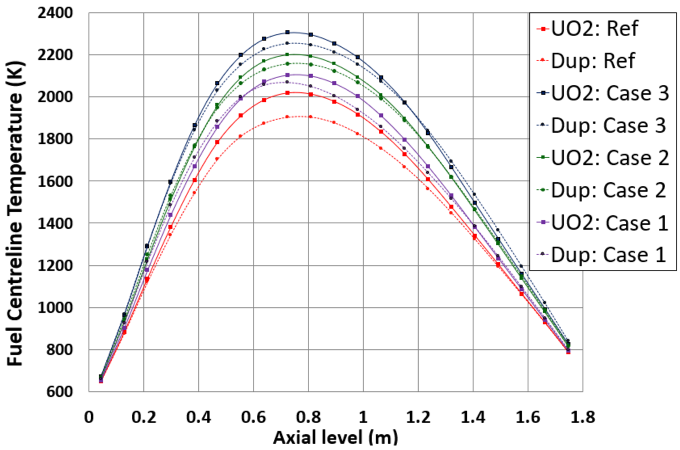


(a)

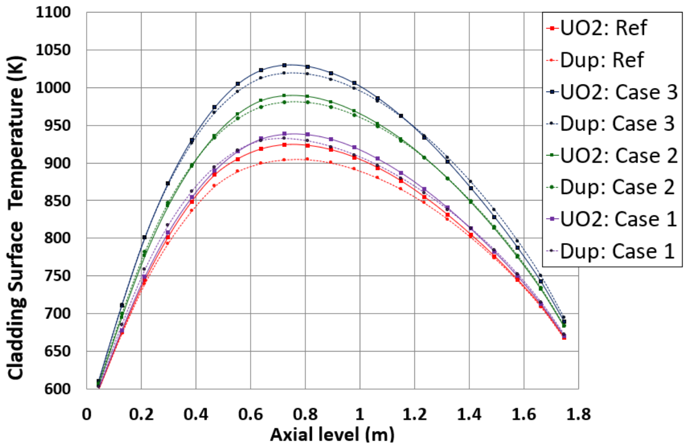


(b)

Fig. 7. (a) Surface heat flux in the hot channel; (b) Relative maximum CHF and SHF along the hot channel vs. coolant mass flux.



(a)



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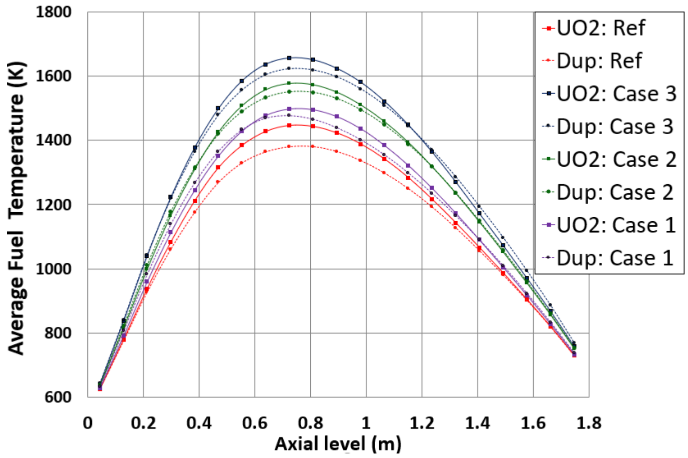


(c)

Fig. 8. Temperature distributions in the hot channel for different HPD cases: (a) Fuel centreline; (b) Cladding inner surface; (c) Average fuel temperatures.

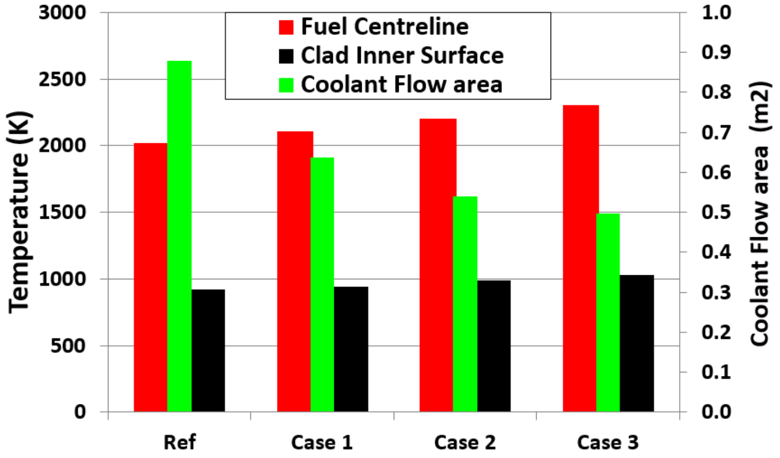


Fig. 9. Variation of coolant flow area and fuel and cladding temperatures for UO_2 fuel for the cases considered.

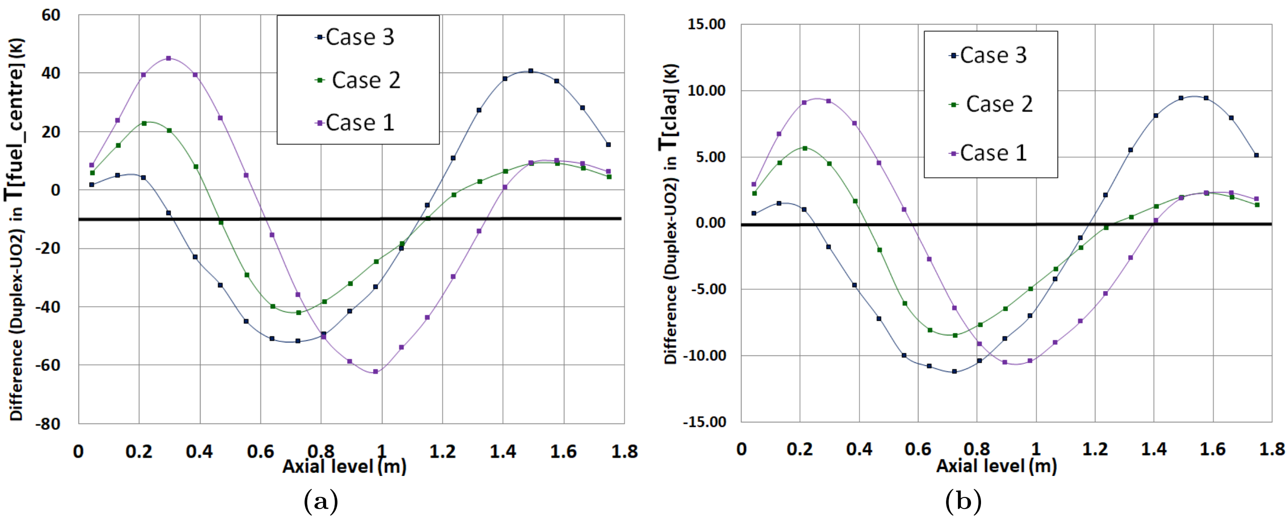


Fig. 10. Differences (K) in temperatures between duplex and UO_2 fuel for different HPD cases: (a) Fuel centreline temperatures; (b) Cladding inner surface temperatures.

temperatures are higher for the UO_2 fuel in the middle of the rod. This can be explained by the similar behaviour exhibited by the SHF for both fuels.

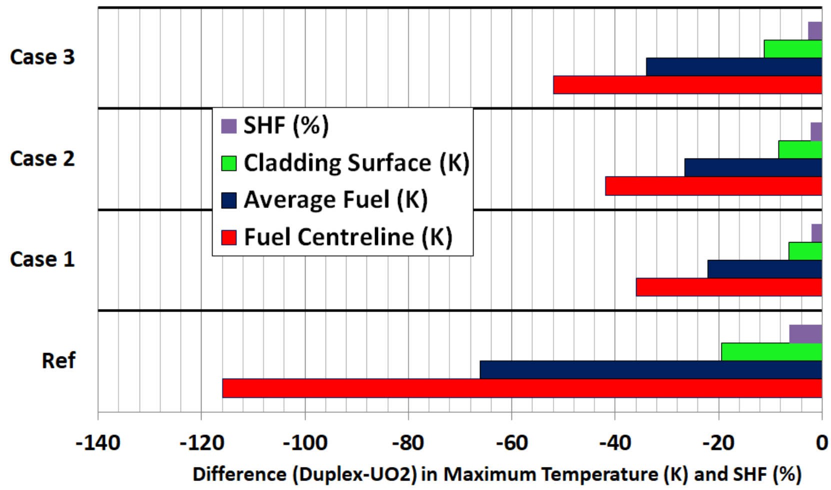


Fig. 11. Differences in SHF (%) and in peak temperatures (K) between UO_2 and duplex fuel for different HPD cases.

Fig. 11 shows that, for all the cases considered, the maximum fuel centreline, average fuel and cladding surface temperatures along the fuel rod are higher for UO_2 than duplex fuel. This can be attributed to the maximum SHF values experienced by the candidate fuels for each case.

It is also important addressing that Table 1 shows the parameters for the candidate HPD cases while assuming the constant mass flow rate and system pressure. As the design focus is to reduce the core volume while keeping the mass flow rate fixed, ΔT of the coolant across the core and the core average temperature will be higher, which keeps the fuel temperature, CHF and DNBR within the safe margin. The coolant inlet and outlet temperatures for UO_2 and duplex fuels for different cases are presented in Table 4. The relative change of ΔT for the best design scenario (Case 2) for the UO_2 and Duplex fuels are $\sim$7% and $\sim$10%, respectively. This higher ΔT will require a higher capacity steam generator to extract heat from the core. Designing the primary, secondary heat exchanger and associated system and estimation of the modified elevation of the primary circuit for proper natural circulation during accidental conditions are the future scopes of this current study.

5.3. Pressure drop

The pressure drop across the core, which has an important impact on pumping power requirements, must also be examined. A larger core pressure drop reduces the reflood speed, which can lead to the violation of cladding surface temperature limits. In steady-state TH analysis, the recommended pressure drop limit for pumping capacity for a small PWR core is $\sim$50 kPa (Greenspan, 2005). We adopt this limiting value in this study.

Fig. 12 shows that in Cases 1 and 2 the pressure drop is below the limit specified, and the pressure drops are almost identical for both fuels in all cases. The pressure drop across

UO_2				
Temp (K)	**Ref.**	**Case 1**	**Case 2**	**Case 3**
Inlet	588.46	589.77	590.17	590.88
Outlet	605.12	607.29	608.05	609.1
ΔT	16.66	17.52	17.88	18.22
ΔT relative change	0	5.16%	7.32%	9.36%

Duplex				
Temp (K)	**Ref.**	**Case 1**	**Case 2**	**Case 3**
Inlet	587.58	589.79	590.09	590.65
Outlet	603.69	607.15	607.91	608.95
ΔT	16.11	17.36	17.82	18.3
ΔT relative change	0	7.76%	10.61%	13.60%

Table 4. Coolant inlet and outlet temperature for UO_2 and Duplex HPD cores.

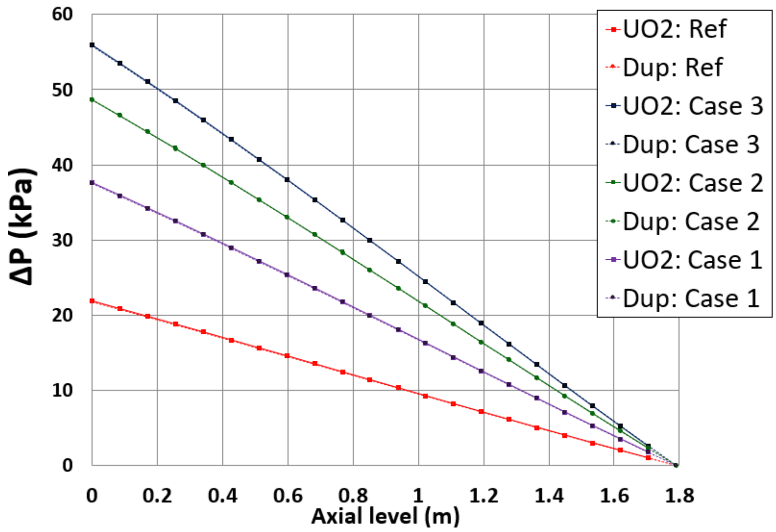
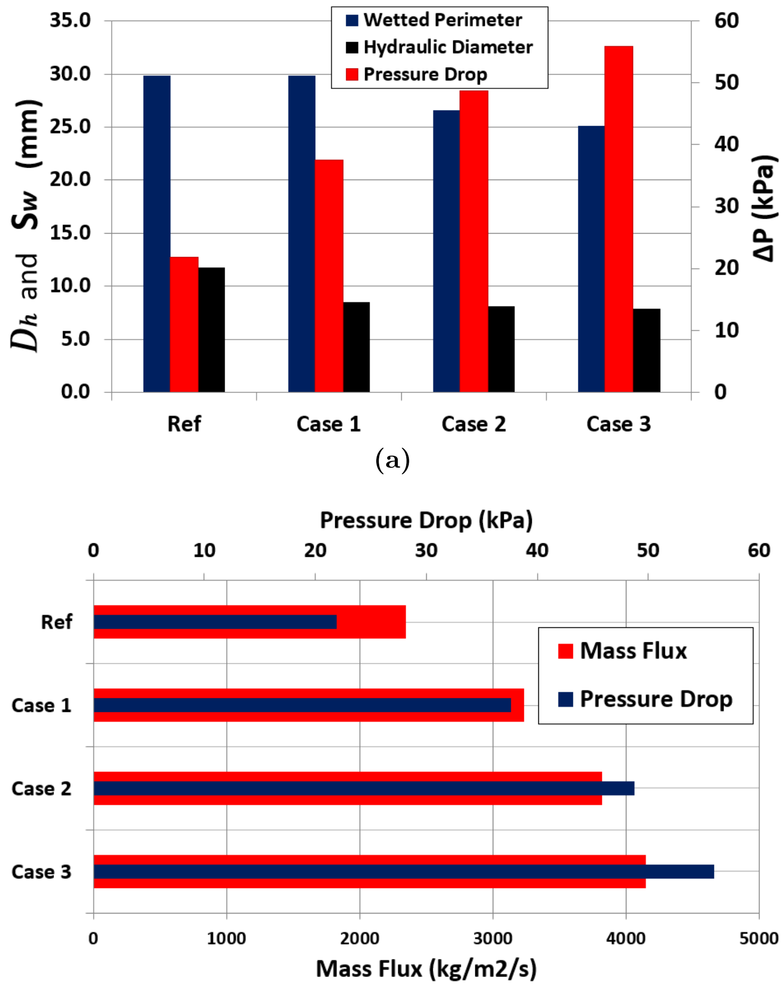


Fig. 12. Pressure drops in the hot channel for different HPD cases.



(a)

(b)

Fig. 13. (a) The effect of hydraulic diameter and wetted perimeter on pressure drop in the hot channel for UO_2 fuel; (b) Hot channel pressure drop and mass flux for the different HPD cases for UO_2 fuel.

the core increases with increasing power density due to the lower hydraulic diameters (D_h) arising from the reduced wetted perimeters (S_w), leading to higher mass flux (Fig. 13a). Cases 3 and 1 give the highest and lowest core pressure drops, respectively, due to their having the highest and lowest mass fluxes, as shown in Fig. 13b.

The Case 3 core pressure drop of ~55 kPa is ~10% higher than the 50 kPa limit. Although Case 3 offers a power density of 111 MW/m^3, this core design will require a high pumping power (as this is directly proportional to the core pressure drop). Note that the form loss coefficients associated with the total pressure drop loss at the entrance (lower core plate and assembly bottom nozzle) and exit (assembly upper nozzle and core upper plate) are not considered in the current HC analysis. These parameters have an effect on overall core pressure drop and should be taken into account in future detailed design studies.

Finally, from the foregoing, we can conclude that Cases 1 and 2 (with power densities of 82 and 100 MW/m^3) satisfy all the TH design goals and performance criteria specified. It is also important addressing that decreasing P, D and P/D for realizing the HPD core environment results in reduced flow channel area and hence higher coolant pressure loss across the core (as well as the natural circulation capability). However, considering the modified metal-to-water ratio and another modified design parameter such as the reduced fuel mass (%), the current study shows that the maximum coolant pressure drop for the cases satisfies the design margin. Here, the system pressure and coolant mass flow rate are considered fixed, and pressure drop for the successful case (Case 2) has not increased significantly. Therefore, the required pumping power will not be significantly higher. As addressed, natural circulation during accidental condition and primary to secondary heat transport are the future scope of the current study.

6. Discussions on practical considerations for the duplex fuel for the HPD Cores

The major challenge for the micro-heterogeneous duplex fuel arrangements is to meet the TH margins since the most severe situation will be in the duplex pellet case where most of the power is generated in the UO_2 part of the fuel pellet. In order to confirm that all the TH constraints are satisfied for the duplex fuel, coupled neutronic/thermal-hydraulic hot channel analysis has been performed for the HPD cases to evaluate key TH parameters. The results confirmed that TH design requirements for the duplex fuel can be met and there will be no melting in the UO_2 region of the duplex pellet for the HPD cases up to 100 MW/m^3. Since TH design requirements are met by a good margin, it can, therefore, be expected that other issues (e.g. hydriding of cladding, fission gas release, and pellet/cladding mechanical interactions) arising from the large temperature gradients in the UO_2 part of the fuel pellet can be avoided.

Our neutronic design studies of long-life HPD candidate cores (Alam, 2018, Alam et al., 2018c,d) showed that the normalized power of duplex pellet (at the UO_2–ThO_2 interface) is about a factor of 1.4 at the BOL and almost 20% higher than the all-UO_2 fuel. It is clear that power peaking can be kept below standard industry limit of 1.5 (Pramuditya and Takahashi, 2013). It is worthwhile mentioning that previous studies (Alam, 2018, Shwageraus et al., 2004) exhibit a normalized power of 2.4 for axially micro-heterogeneous duplex fuel and this higher BOL normalized power UO_2 region results in an unacceptably high fuel temperature. In order to avoid this issue, our radial micro-heterogeneous duplex fuel for the HPD cores is designed in such a way that ThO_2 region is ∼25% of the UO_2 region and normalized power is limited to 1.4, which is an obvious design improvement considering the practical perspective. A detailed discussion of the radial micro-heterogeneous duplex fuel design and analyses can be found in the first author's PhD research (Alam, 2018).

In addition to satisfying the neutronics and TH requirements and constraints, additional burnup requirement needs to be satisfied. Cladding oxide thickness of 100 μm currently limits discharge burnup up to 62 GWd/tHM, and therefore new accident-tolerant cladding will be required (Alam et al., 2019, 2018a,b). Fission gas release is limited to a few percents for up to 50–60 GWd/tHM, but can easily exceed 30% for the higher burnup. Fuel swelling may accelerate and burst the cladding. Fuel melting temperature and thermal conductivity are reduced as well with higher burnup. However, It is worthwhile addressing that it will take a long time to achieve widespread commercialization of civil nuclear marine propulsion. In addition, rapid improvements in materials and fabrication of accident-tolerant fuel can lead to permitting ever-higher burnup (Alam, 2018, Otto, 2013). Therefore, In this study, higher discharge burnup isn't considered as a constraint, rather it is an objective to ensure long life operation. However, these issues are out of the scope of this paper.

It is also worth addressing that an incremental improvement and further investigations on the proposed civil marine core design are still in progress. The fuel performance issues will be considered for the HPD cases to evaluate the fuel rod thermal and mechanical performance. In the previous study (Alam, 2018), we have considered the issues of preliminary assessment of fuel rod performance of HPD cores indicating that the main requirements are met and these issues are, however, out of the scope of this paper.

7. Conclusions and Future Works

Coupled neutronic/thermal-hydraulic hot channel analysis has been performed to investigate the feasibility of a HPD marine PWR concept and to identify the main TH challenges characterizing proposed core designs. Three HPD design cases have been studied for two candidate fuels to evaluate key TH parameters such as: MDNBR, surface heat flux, critical heat flux, cladding inner surface and fuel centreline temperatures, and pressure drop. The most important findings of this paper is that the average core power density for the SMR (60–65 MW/m^3) can be increased up to 100 MW/m^3 while satisfying the TH parameters. It has been observed that

- MDNBR will not be a limiting factor for these HPD designs. DNBR values are higher for the UO_2 fuel than for the duplex fuel at the lower axial levels of the fuel rod due to the higher SHF experienced by the duplex fuel. Things are different in the upper axial levels of the fuel rod where UO_2 SHF values are larger and, therefore, DNBR values are marginally higher for duplex fuel. DNBR values are consistent with SHF values.

- SHF values in the hot channel for Cases 1 (82 MW/m^3) and 2 (100 MW/m^3) are far from the limit of 1.57 MW/m^2, unlike Case 3 (111 MW/m^3). However, Case 3 does not violate the MDNBR limit for either fuel.

- The maximum fuel centreline, average fuel and cladding surface temperatures along the fuel rod are seen to be marginally higher for UO_2 fuel than for duplex fuel. Cases 1 and 2 satisfy the cladding inner surface and average fuel temperature limits, but Case 3 violates the fuel temperature safety margins for both candidate fuels.

- Cases 1 and 2 satisfy the maximum permissible pressure drop limit specified, but Case 3 exceeds this limit.

- Case 3 (111 MW/m^3) exceeds the permissible SHF, fuel temperature and pressure drop limits, but the Case 2 core design (100 MW/m^3) satisfies all the neutronic and TH constraints specified. The average core power density for Case 2 is increased by $\sim$50% compared to the reference core design (63 MW/m^3) and is comparable that of the Sizewell B PWR (101.6 MW/m^3). This means that capital costs could be reduced and the economic attractiveness of the marine core will be improved. It is worthwhile addressing that core power density is the parameter that is considered in this study in terms of thermal-hydraulics safety assessment, which affects capital costs and economics of the SMR. Obviously, the capital cost depends on many factors and parameters, and always there is some trade-off.

One of the important safety aspects is fuel performance issues which needs to be investigated in the future. It is recommended that fuel performance analyses for the HPD fuels be undertaken to verify the integrity of the fuel rods for the proposed HPD cores including:

- Integral fuel rod performances and fission gas release (FGR);

- Pellet-clad interaction (PCI) phenomena caused by excessively high rod internal gases and pellet swelling;

- Oxide thickness (corrosion);

- Flow-induced vibration.

It is also required to consider various uncertainties for the future work:

- The uncertainty of plant operating parameters (e.g., flow rate, bypass flow rate, pressure, power) need to be deterministically or statistically considered to calculate DNBR by sub-channel analyses.

- The uncertainties of sub-channel area, resulting from fabrication tolerances of rod diameter and position as well as rod bow needs to be considered.

- The uncertainty of nuclear data needs to be considered in the rod power calculation.

Future work will also include evaluation of the effect of the HPD lattice on the reflood phase of a large-break LOCA and the performance of the reactor coolant pumps. The form loss coefficients associated with the total pressure drop loss across the core will also be considered.

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