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for Next Generation PWRs:
Final Report**

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EXECUTIVE SUMMARY

This summary provides an overview of the results of the U.S. DOE funded NERI (Nuclear Research Energy Initiative) program on development of the internally and externally cooled annular fuel for high power density PWRs. This new fuel was proposed by MIT to allow a substantial increase in power density (on the order of 30% or higher) while maintaining or improving safety margins. A comprehensive study was performed by a team consisting of MIT (lead organization), Westinghouse Electric Corporation, Gamma Engineering Corporation, Framatome ANP (formerly Duke Engineering) and Atomic Energy of Canada Limited. The study involved the evaluation of the new fuel in terms of thermal hydraulic, neutronics, fuel performance including first scoping irradiation tests at the MIT reactor, fuel manufacturing and economics.

The proposed fuel is of annular shape and has both internal and external cooling, as shown in Figure ES-1. To provide sufficient flow rate through the inner cooling channel, significantly larger rod size than the typical fuel rods of 17x17 PWR fuel arrays has to be employed. Therefore, for a fixed assembly size, the PWR fuel assembly has a smaller number of fuel rods.

Figure ES-1 Schematic of solid and internally and externally cooled annular fuel (not to scale)

A transition from solid to annular geometry has two important implications that allow power density increases: (1) reduction of conduction path thickness, which improves margin from peak fuel temperature to melting and (2) increased heat transfer surface area (in spite of a reduction of the number of fuel rods), which improves the margin for Departure from Nucleate Boiling Ratio (DNBR).

The overall objective of this NERI project was to examine the potential for improving safety and economics of pressurized water reactors (PWRs) through a high-performance externally and internally cooled annular fuel. This has been pursued through the following tasks:

1. Identify the most promising fuel assembly arrangement for PWRs to achieve a significant increase in power density of at least 30 percent; based to a large extent on the extensive PWR UO_2 fuel database to minimize R&D development expenses and the risks associated with transition to a new fuel materials.

- Optimize the fuel for superior thermal hydraulic performance. Examine flow distribution, core pressure drop, departure from nucleate boiling ratio (DNBR), and resistance to parallel channel instabilities.
2. Perform safety analyses, such as loss of coolant accident (LOCA) analyses, to confirm safety benefits for the optimum configuration identified in item (1).
 3. Evaluate the neutronic fuel design to achieve high reactivity-limited burn-up and a refueling cycle comparable to current PWR practice to attain good economic features. Confirm the acceptability of the coefficients for reactivity feedback and reactivity control.
 4. Select fabrication processes to produce annular fuel elements with the required product characteristics, including fissile loading and high integrity cladding, which are capable of eventual scale-up into efficient production processes for economic and reliable fuel element performance.
 5. Evaluate the materials and mechanical performance of UO_2 fuel forms obtained by production technologies different from current U.S. practices (e.g., vibropacked fuel), and operating under new conditions (such as very low fuel temperature). Develop models for assessing fuel performance as well as for scoping irradiation tests performed at the research reactor of Massachusetts Institute of Technology (MIT).
 6. Estimate the economic cost or benefit in cases of using the annular fuel for uprating current Generation II PWRs or in new advanced PWRs.

Next, the key results and conclusions are summarized and guidelines as to in which chapters of this report contain more detailed analyses are provided.

Thermal hydraulic performance

The optimum square lattice annular fuel design that allows maximum power uprate was investigated using a VIPRE-01 whole core model. The VIPRE-01 methodology for the analysis of fuel geometry with internal and external cooling was developed. Various square array sizes (11x11 to 15x15) that fit in the fixed dimensions of a fuel assembly were explored. The most promising options, based on DNBR considerations, were found to be the 13x13 and 12x12 arrays with the dimensions shown in Table ES-1. Because fuel performance modeling results showed that thermal expansion and swelling of fuel pellets during operation are towards the outer cladding, the heat flux to the outer channel may increase. The 13x13 array accommodates higher heat flux to the outer channel, thus the 13x13 array was selected as the most promising design.

The 13x13 design allows a power uprate of 50% in terms of DNBR limit*, if the flow rate is increased proportionally. This is a significant power uprate, raising the extracted power from the same core size to support increasing the plant output from the current 1150MWe to 1750MWe. The same power uprate capability was also shown to be possible for the largest currently available reactor coolant pumps, which deliver flow rates limited to 130 percent, if the core inlet temperature is reduced by 10°C. Similar power uprates with annular fuel were found possible for the hexagonal lattice cores typical of Russian VVER reactor designs. At such high power, the peak fuel temperature is still about 1300°C lower than the reference solid fuel, as shown in Figure ES- 2.

Table ES-1 Dimensions (cm) of annular fuel elements of optimum arrays and the reference solid fuel array

Array	D _{cii}	D _{cio}	D _{fi}	D _{fo}	D _{ci}	D _{co}	Pitch
12x12	0.9533	1.0676	1.08	1.5400	1.5524	1.6667	1.789
13x13	0.8633	0.9776	0.99	1.4100	1.4224	1.5367	1.651
17x17-ref.	Solid pin	-	-	0.8255	0.8379	0.9522	1.263

*Subscripts, *ci*, *f* and *co* designate inner cladding, fuel and outer cladding respectively; a second subscript designates outer (o) or inner (i) surface.

Figure ES-2. Comparison of hot-spot radial temperature profiles for solid and annular fuel

* Core with annular fuel operating at 150% power gives the same minimum DNBR as the reference core with solid fuel at 100% power.

Such a high power uprate requires proportional increase of core flow rate resulting in a larger pressure drop and velocity, which in turn raises concerns about vibration and fuel assembly lift-off. To address vibration concerns at higher velocities, five flow-induced vibration mechanisms for the annular fuel rods were analyzed. Compared to the reference solid fuel, the annular fuel was found to exhibit, at 150% flow rate, better mechanical behavior with respect to vortex-induced vibration, fluid-elastic instability, and turbulent-induced vibrations than the reference solid fuel in a 17x17 lattice at 100% power. Also, fretting, sliding and impact wear were found to be smaller for annular fuel. Annular fuel benefits from smaller cross flow velocities and more importantly larger rigidity. Analysis of the buckling instability of the inner cladding, which lacks grid support, showed that the maximum stress in the inner cladding generated by the bending moment is small due to very limited cladding displacement constrained by the fuel and the outer grid-supported cladding. Evaluation of acoustic resonances of the inner channel cladding with pump blade passing frequencies showed that there is a need to slightly increase separation of the natural frequency from the pulsation frequency. This can be done either through natural frequency change by increasing the length of fuel pins or through a change in pulsation frequency. The latter can be achieved by lowering pump speed or the number of blades. Overall, the proposed annular fuel design was found to be more resistant to the above vibration modes and vibration issues do not pose a limit to power and flow rate increase of 50%. However, lift-off forces are several times higher than for the reference fuel and will require design modifications of the fuel assembly and fuel rod holding mechanism. Details on mechanical design are in Chapter 8.

Because parallel non-communicating channels are more susceptible to various thermohydraulic instability phenomena than communicating channels, flow instability concerns were addressed. Two major instability types – excursive (Ledinegg) instability and density wave oscillations – were evaluated. No excursion type instabilities were observed and MDNBR was found more limiting than the flow excursion instability limit. The analysis of susceptibility to density wave oscillations confirmed that operation at 150% power is deeply in the stable region. Therefore, flow instability is not an issue for the proposed annular fuel, in spite of the fact that the internal channels are isolated and do not communicate with other channels. The high system pressure and the small channel fraction experiencing subcooled boiling with a small void fraction, are the main factors for operation far from the instability line.

Sensitivity calculations have shown that the MDNBR is more sensitive to manufacturing tolerances and oxide growth than the reference solid fuel, because of the absence of cross flow mass and enthalpy exchanges in the inner channels. Nevertheless, sensitivity results have also shown that MDNBR changes due to these effects are small and do not violate the MDNBR limit. In addition, annular fuel is more sensitive to operating parameters, such as core flow rate, core power, core inlet temperature and systems pressure. This larger sensitivity is compensated by the larger DNBR margin at rated conditions gained from the new geometry with larger heat transfer surface and higher mass flux. Finally, the concern about potential plugging of inner channel with debris was also investigated. Although total plugging of the inner channel does not lead to excessive fuel temperature, the inner cladding would be subjected to damaging temperatures, hence full plugging must be prevented. This can be easily achieved through the use of inlet debris filters,

which are used in all current PWRs to minimize debris-induced fretting and have mesh passages smaller than the diameter of the inner channel.

In conclusion, steady thermal hydraulic calculations confirmed that the proposed annular fuel can achieve substantial power uprate up to 50% and no show stoppers were identified. Details of these thermal hydraulic studies are given in Chapter 6.

Safety Analysis Performance

Four accidents of the annular fueled PWRs were evaluated in a comparative manner against the reference PWR plant with traditional solid fuel. The accidents involved the Loss-of-Coolant Accident (LOCA), the Loss of Flow Accident (LOFA), the Main Steam Line Break (MSLB), and the Rod Ejection accident. To perform these analyses, RELAP5-3D/ATHENA was used, after proper models were developed.

To accommodate 50% higher decay heat during reheat period after LOCA, three options of safety injection (SI) and accumulator (ACC) systems have been explored involving 100% SI and 150% ACC, 150% SI and 100% ACC and 150% SI and 150% ACC. The blowdown cladding temperature peak was eliminated for all cases even at 150% power because of very low stored energy in the fuel. The peak cladding temperatures from heatup remained well below the safety limit of 1200°C for all three cases, but for the case with 100% SI, the peak cladding temperature stayed at high values much longer than the other two cases before quenching. Therefore, SI at 150% capacity and the same accumulator size (100% capacity) were selected for the 150% rated core. Overall, the analyses confirmed that the core at 150% power could withstand a LOCA without exceeding the cladding temperature limit.

The complete Loss of Flow Accident (LOFA) was analyzed using RELAP3D/ATHENA code and VIPRE-01 code. The results at 150% power show that the DNBR of annular fuel decreases faster than that of the reference solid fuel at 100% power, mainly due to its larger sensitivity to coolant flow rate. However, because the initial DNBR margin of annular fuel is significantly larger than for the solid fuel, the lowest value of MDNBR reached during the transient is slightly higher than that of the solid fuel. After reaching a minimum value, the MDNBR for annular fuel increases more rapidly than that of solid fuel due to the much smaller stored energy of annular fuel. The LOFA results confirmed the results of the steady-state MDNBR analyses at overpower, which showed the capability of annular fuel to operate at 150% power.

The Main Steam Line Break (MSLB) and Rod Ejection Events were analyzed for solid and annular fuel rods at 100% and 150% power levels. The MSLB results confirmed the expectations that the annular fuel operated at 150% power has sufficient MDNBR margins comparable to those of the solid fuel at 100%. Also, the rod ejection results showed that the peak fuel temperature for the annular fuel would remain significantly below that for the solid fuel (about 1200°C versus 2200°C). On the other hand, the temperature increase from steady state values was about 50% higher for the annular fuel due to higher power density and slightly smaller fuel volume. Nevertheless, the radial averaged fuel temperatures of annular fuel remains significantly lower than that of the solid fuel (1150°C versus 1450°C). Correspondingly, the radial average fuel enthalpy at the axial location of the peak temperature is about 30% lower for the annular fuel than for

the solid fuel. Therefore, the margins to fuel enthalpy limit is substantially higher for annular fuel in spite of its 50% higher power density.

In conclusion, the safety analysis results of the above four major accidents show satisfactory safety performance of annular fuel for power uprates up to 50%. More details on safety analyses are given in Chapter 7.

Neutronic performance

The optimum dimensions of the internally and externally cooled annular fuel are primarily determined by thermal hydraulic analyses, which were performed under the constraint of maintaining the heavy metal to moderator ratio close to the reference value of the current solid fuel. Additional cladding volume is needed for the annular fuel, which reduces the volume available for the fuel and coolant. Hence, about 9% less heavy metal can be loaded in the core, which affects negatively the fuel cycle length. Nevertheless, the heavy metal to moderator ratio was preserved by correspondingly slight reduction of moderator volume. The smaller heavy metal loading and added cladding necessitate slightly higher enrichment, but this increase is minor in comparison with the enrichment increases needed to achieve 50% higher burnup as a result of large power uprate to maintain the cycle length of 18 months per current practice in most US plants. Therefore, the major challenge in the neutronic task was to design the core and its fuel management scheme such that it can achieve 18 month cycle length at 50% higher power density with acceptable power peaking and other neutronic parameters. The design targets include assurance of core operation at a 90% capacity factor for an 18-month cycle, maximum core boron concentration of 1750 ppm, maximum radial pin power peaking of 1.65, and a hot spot factor of 2.5.

Three cores were designed using the CASMO4/SIMULATE code package and a consistent methodology – a reference PWR core with solid fuel and two annular fueled cores, one operating at 100% power and the other at 150%-power. This made possible a comparison between the annular-fueled cores and the reference core based on the same number of reload assemblies and the same set of design criteria. The generic annular core design philosophy followed the existing industrial 3-batch core. The 72 reload assemblies were subdivided into two enrichment levels: 48 with higher and 24 with lower enrichment. The sub-batch with higher enrichment stays in the core for 3 cycles whereas the lower-enriched fuel remains for 2 cycles, but at higher flux regions to achieve comparable burnup. The loading pattern of an equilibrium annular fueled core for 150% power rating is shown (with peaking and burnups) in Figure ES-3. The red, blue and grey colors indicate fresh, once-burnt and twice burnt fuel assemblies, respectively (corresponds to first number 0,1,2). The letters H and L designate high (9.0%) and low (8.1%) enrichment, the first two numbers after a letter stand for number of rods with gadolinium and the last two numbers designate Gd wt% in these rods (for example 80 stands for 8.0wt%).

Figure ES-3 shows that fuel assemblies are exposed to significantly high burnups, up to 102MWd/kg, although accumulated over the same 4.5 year core residence time as for the reference solid fuel. To extract ~50% more energy per assembly requires a corresponding increase of enrichment. The 8.1% and 9.0% are well above the 5 w/o licensing limit of

most manufacturing plants, hence a transition to this annular high power density fuel would require changes in fuel manufacturing plants. However, if heavier uranium compounds can be considered, e.g., uranium nitride, and the number of reload assemblies can be increased, the fuel enrichment can be limited within 5 w/o. Higher enrichment hardens the neutron spectrum and as a consequence reduces the reactivity worth of control materials. Satisfying the limit on core boron concentration implies more burnable poison usage in the fresh fuel. The management of the power distribution through the entire cycle while ensuring acceptable burnable poison residue becomes more challenging. The shutdown margin is also slightly reduced based on the existing Ag-In-Cd control rod system and requires stronger control materials.

	H	G	F	E	D	C	B	A
08	2L2410 0.968 1.001 94.289	0L2410 1.350 1.470 36.666	1L2880 1.124 1.175 73.573	0L2880 1.313 1.422 38.982	2H2880 0.947 0.988 102.349	0H2880 1.334 1.440 41.055	1L2410 0.980 1.111 67.491	2H2880 0.523 0.740 87.522
09	0L2410 1.350 1.470 36.666	2H2880 1.037 1.091 92.819	0L2880 1.332 1.441 38.588	2H2880 1.028 1.096 89.111	0H2880 1.302 1.412 40.261	1L2880 1.051 1.118 73.400	0H2880 1.267 1.425 36.963	1H2880 0.682 0.969 56.726
10	1L2880 1.124 1.175 73.573	0L2880 1.332 1.441 38.581	2H2880 1.004 1.053 93.549	0L3280 1.260 1.369 39.024	2H4060 0.926 1.018 88.512	1H2880 1.030 1.116 72.518	0H2880 1.259 1.418 37.033	1H4060 0.665 0.948 56.883
11	0L2880 1.313 1.422 38.982	2H2880 1.028 1.096 89.077	0L3280 1.259 1.367 38.976	1H1660 1.051 1.182 69.067	1L3280 0.912 0.962 72.097	1H2460 1.026 1.112 69.825	0H2460 1.127 1.346 34.212	2H2460 0.423 0.709 81.499
12	2H2880 0.947 0.988 102.349	0H2880 1.302 1.412 40.259	2H4060 0.925 1.018 88.548	1L3280 0.912 0.962 72.055	1H2880 0.988 1.084 75.714	0H4060 1.222 1.358 37.698	1H2880 0.726 0.968 62.357	
13	0H2880 1.334 1.440 41.055	1L2880 1.051 1.118 73.390	1H2880 1.030 1.116 72.519	1H2460 1.026 1.112 69.864	0H4060 1.223 1.359 37.755	0H1660 1.072 1.343 32.279	2H2880 0.395 0.694 86.882	
14	1L2410 0.980 1.111 67.491	0H2880 1.267 1.425 36.957	0H2880 1.259 1.417 37.035	0H2460 1.127 1.346 34.243	1H2880 0.729 0.970 62.465	2H1660 0.413 0.711 80.672		
15	2H2880 0.523 0.740 87.522	1H2880 0.682 0.969 56.713	1H4060 0.665 0.947 56.934	2H2460 0.423 0.709 81.545	Fuel ID Assembly power Peak pin power Assembly burnup			

Figure ES-3: Assembly power distribution at EOC for the IXAF-fueled, 150%-power core

Reactivity feedback parameters were also evaluated for all three cores. The moderator temperature coefficient of the annular core is slightly more negative than for the solid core. The Doppler coefficient is about the same, but the lower initial temperature of the annular fuel provides larger margins for negative Doppler feedback in accidents with reactivity increase, and smaller reactivity penalty for zero to hot full power heat up.

The potential of plutonium burning in the high power density core with annular fuel was also explored. The annular fertile free Pu loaded fuel in a highly uprated core appears to be feasible, but power uprates are restricted to less than 50% because of relatively high power peaking and potential for slightly positive MTC at BOC. The results suggest that a power density increase of 40% should have the initial Pu loading sufficiently low to assure acceptable core power peaking and negative MTC at all times during the cycle. However, the need for a large amount of enriched burnable poisons makes the design of fertile-free Pu burning core economically unattractive. A more attractive option for management of the transuranic isotopes deserving evaluation in the future would be the COMbined Non-Fertile and UO₂ (CONFU) fuel, which could be easily adapted for annular fuel.

Overall, the annular-fueled high power density cores showed comparable steady-state performance, including power distributions and reactivity coefficients, to the reference solid fuel core. Hence, the retrofittability of the new annular fuel appears to be technically feasible for the next generation high density PWRs from a reactor physics point of view. More details on reactor physics analyses are covered in Chapter 9.

Fuel Fabrication Feasibility

One of the major issues of any new fuel is the feasibility of its fabrication to high and stringent standards. Therefore, the important task of this project was focused on the evaluation, development, and laboratory-scale demonstration of selected, desirable fabrication processes of the annular fuel. Fuel fabrication feasibility study was performed in three phases. First, a broad based evaluation of the possible fabrication processes that are pertinent to the optimized annular fuel design were performed by Gamma Engineering.

Potential fabrication routes and processing technologies of annular fuel elements that were evaluated included:

- (1) Sintered ring pellet fabrication route with current commercial punch and die pressing technology for fabricating green pellets followed by sintering processing.
- (2) Sintered ring pellets fabrication route with slurry extrusion technology for fabricating green pellets followed by drying and then sintering processing.
- (3) Sintered ring pellets fabrication route with cold iso-static pressing technology for fabricating green ring pellets followed by sintering processing.
- (4) VIPAC fuel element fabrication route with different particle size components of crushed high density sintered UO₂ fuel material.

- (5) VIPAC fuel element fabrication route with different particle size components of spherical high density UO_2 fuel material formed by special processes such as sol gel.

The sintered ring pellet (1) and VIPAC (4) fabrication technologies were selected as the most promising candidates for further evaluation.

In the second phase, analyses and development of the above most promising fuel fabrication technologies were pursued. The third phase covered laboratory-scale demonstration of the developed fabrication processes to provide near prototypical annular fuel rods with depleted uranium oxide for pertinent product characterization. The major results achieved for the sintered ring pellet and VIPAC processes are summarized below.

VIPAC fuel fabrication was performed at AECL. First, six 10cm long annular fuel specimens with enriched uranium fuel were fabricated to be used at MITR for irradiation – see Figure ES-4. High density ($>97\%$ theoretical) crushed 4.91wt% enriched sintered UO_2 pellets were used as feed material. Due to the small annulus gap width for fuel material (i.e., ~ 2 mm), the optimum fuel feedstock was found to require two particle size components (i.e., ~ 70 wt% of 0.25-0.50 mm granules and ~ 30 wt% 0.025-0.053 mm granules) for achieving the highest smear density in the finished product. The maximum smear density attained by vibration packed processing only was $\sim 77\%$ theoretical. An increased density of 82% was achieved using a mechanical impact processing technique with increased force developed by AECL. Additional density increase to 88% was achieved by mixing 15% uranium metal powder with UO_2 . The metal powder size was 0.25- 0.50 mm.

Figure ES-4 Annular fuel specimens for irradiation at MITR

Next, two four foot lengths of annular fuel were manufactured and successfully loaded using the VIPAC approach to achieve a maximum density of 77% of theoretical. This is insufficient to meet the reference fuel loading requirements of an effective uranium oxide density of at least 85% of theoretical UO_2 density. It was also observed that the granules compacted to a certain density of approximately 72% relatively quickly and after this point small increases in density were only achieved following extensive compaction.

Therefore, further exploration of VIPAC fuel was not pursued, but it was noted that higher densities may be achieved through an addition of a uranium metal powder component to the oxide particle fuel mixture before compaction. Hence, VIPAC fuel fabrication route involving addition of metallic uranium powder may be a backup option for the more promising sintered pellet route described below.

Sintered ring pellets for the 13x13 annular fuel rods were manufactured at the Westinghouse Nuclear Fuel Company in Columbia, South Carolina. Seventy green annular pellets were produced and sixty of these pellets were then sintered. Figure ES-5 shows some of the sintered annular pellets. Properties of thirty sintered pellets were then extensively measured and the OD surface of the pellets were then finished utilizing a commercial centerless grinder. The overall yield of the thirty selected pellets from powder pressing, sintering and OD surface grinding was 97% with the loss of one pellet during grinding. There were no technical issues with the fuel pellet fabrication or OD surface grinding. The crucial pellet dimensional tolerances met the preliminary fuel design specifications.

Figure5 Sintered pellets manufactured at Westinghouse

Based on this Westinghouse testing work, the press-and-sinter pellet fabrication route appeared to be the most promising technology for commercially manufacturing the reference annular fuel. Therefore, a second set of annular pellet fabrication and rod loading trials, using commercial fabrication techniques, was arranged by Westinghouse and performed at INVAP (located in S.C. de Bariloche – Río Negro Province, Argentina). The second batch of pellets of about 200 that was produced showed that the desired tolerances can be achieved. The conical shape of the annular pellets has been substantially reduced. Further, end plugs were designed and welding techniques successfully demonstrated. Finally, the 4-foot long rods were successfully loaded with annular pellets and both end plugs welded inside and outside and the rods were pressurized with helium gas.

Overall, the conclusion is that annular fuel elements can be manufactured by commercial fabrication techniques to required specifications using the sintered ring pellet route, at reasonable costs. Therefore, fabrication of annular fuel should not be an inhibiting factor in the commercial introduction of annular fuel to improve the performance of existing light water reactors. The description of fuel manufacturing activities is in Chapter 11.

Economic Feasibility

The new fuel could be introduced only if it can bring significant economic benefits to nuclear power plant operators. Therefore, quantification of the potential economic gain is a key goal of this project, so that a separate task was devoted to economic studies.

Fuel cycle cost and the assessment of the impact of the high-power density annular fuel on plant economy were performed by Westinghouse. The objective was to provide the engineering and economic analyses to allow the annular fuel cost/benefit ratio to be determined. The evaluations were pursued in two areas. The first area focused on manufacturing cost, in particular on the modifications required for the Columbia plant to accommodate manufacturing of annular fuel rods and the associated costs. In the second stage, the benefit from a large power uprate on the projected cost of electricity was assessed.

Fuel manufacturing costs were estimated from an assessment of the manufacturing issues encountered when a central tube is introduced in the annulus of a fuel rod. The baseline constraints and assumptions used reflect the current permitting and operational constraints at the Westinghouse Nuclear Fuels plant in Columbia, South Carolina.

Two types of fuel were considered. The first is an annular pellet fuel similar to but bigger than the UO_2 based annular pellets currently made for WABA rods at Columbia. The second is a particulate fuel using vibration packing of crushed and screened irregular UO_2 particles as has been used by AECL in Chalk River (see Chapter 11) for the test fuel segments.

For annular pellet fuels, the pellets are assumed to be first loaded into the outer tube, as is currently done for solid pellets. This approach is taken since the outside of the pellets can be ground to a very close tolerance. The inside tube is then inserted with a pre-welded bottom closure already attached. The addition of the second tube requires only a minor modification of the current assembly station. The spring and annular closure are then added to the opposite end. Minor increases in manufacturing costs are expected for two new welding stations. The only capital investment is for increasing the sintering furnace capacity due to the lower overall density of the pellets. However, no significant increase in the final cost of fuel is expected, since the pellet cross sectional area does not exceed that of the solid pellets needed in each assembly.

A cost estimate has been made to take into account the following additional capital and additional operating costs due to the manufacture of annular fuel:

1. Product charges for capital costs due to the increased number of sintering furnaces;
2. Product charges for capital costs associated with additional welding stations;
3. Product charges due to increased zirconium usage.

Increases in fuel manufacturing costs were determined from these capital costs and combined with additional zirconium costs to obtain the total marginal cost increase of the annular fuel from the current \$.005/kWhr(e) to about \$.00502/kWhr(e). This cost increase is negligibly small.

Regarding the benefit on plant economics, three plant options using annular fuel were compared to two options using standard solid fuel. The discounted rates of returns for the various options were found to be as follows (listed best to worst):

1. 1,717 MW(e) Generation III new PWR; annular fuel – 11.3%
2. 1,717 MW(e) Generation III new PWR; standard fuel – 10.8%
3. 1,117 MW(e) Generation III new PWR; annular fuel – 7.3%
4. 1,117 MW(e) Generation III new PWR; standard fuel – 6.9%
5. 600 MW(e) uprate to a Generation II PWR; annular fuel – 6.3%

The use of annular fuel always improves the rate of return on invested capital. In the case of a new Generation III power plant, the use of annular fuel can be used to either get more capacity from the same sized nuclear island or to reduce the size of the nuclear island. It appears that the rate of return on money can be increased by 0.4- 0.5% if the new annular fuel is used.

For the currently operating Generation II power plants, annular fuel provides a means to significantly upgrade the power output (up to 50%) using the same nuclear island with new reactor internals, steam generators and circulation pumps, and a new incremental balance of plant (built during plant operation) to handle the increased power production. However, the ROR is less than for the new reactor options. This is mainly due to the high cost of the replacement power and the unused fuel value during the transition from standard to annular fuel. For instance, if these two items were not included, the capital cost would significantly decrease and the value of this option would increase extensively with a ROR of about 11.6%. Therefore, if low cost replacement power is readily available during the transition period and if the unused fuel cost can be reduced due to tighter fuel management before the transition, then the uprate option becomes the most favorable of all options.

Overall, annular fuel appears to be economically attractive in comparison with the solid fuel for any PWR plant. The details of economical studies are given in Chapter 12.

Fuel Performance

The performance of the internally and externally cooled annular fuel during irradiation was evaluated using a modeling approach and also a scoping irradiation test at MITR, as outlined in the two sections below.

The FRAPCON-3 code was modified to develop the annular fuel performance code FRAPCON-ANNULAR, which was then utilized to perform analysis of the irradiation of the hottest pin in the sintered annular fuel design. Comparisons have been made with a reference Westinghouse solid fuel rod operating at its rated power, and an actual commercial PWR rod operated to high burnup. The high power annular fuel would be irradiated (in the model) to a much higher burnup than solid fuel (86 versus 50 MWd/kg).

At this high burnup, fission gas release (FGR) from the highly uprated annular fuel is larger than the reference solid fuel case in spite of its much lower fuel operating temperature. This is because significant fission gas release starts to occur at burnup higher than 45 MWd/kg due to microstructure changes of the fuel. However, total FGR from the annular fuel at 86MWd/kg is still relatively low (less than 6%). Both claddings of the uprated annular fuel experience larger strains during the first hundred days of operation as a result of larger cladding creepdown due to the larger initial diameter, but remain below the 1% limit. The annular fuel cladding oxide thickness and hydrogen pick-up were found to be smaller than for the solid fuel due to lower cladding temperature. At 150% power, the inner cladding had slightly higher oxide thickness than the solid fuel due to its higher heat flux, but still within acceptable limits. Because the high burnup is achieved at about the same cladding temperature and over the same time period as for today's solid fuel, cladding performance of high power density annular fuel is not significantly different from the reference solid case.

One potential issue identified through modeling is the asymmetry of the outer and inner gap conductances and its effect on the heat flux values, as a result of preferential expansion of the pellet ring towards the outside. A number of approaches have been explored to resolve the issue of this gap conductance asymmetry. A promising approach is adding by sputtering an outer fuel layer of ZrO_2 particles. This prevents quick contact between the cladding and the fuel, thus limiting the outer gap conductance due to the presence of a barrier of a smaller thermal conductivity at the time of outer gap closure, if the inner gap is open. After inner gap closure, the ZrO_2 at the outer surface is crushed due to a large contact pressure and the outer gap thermal conductance is increased closely matching that of the inner gap. Coverage of 50% of the outer fuel surface with ZrO_2 particles in combination with typical cladding and fuel roughnesses of 3 μm was found to yield excellent gap conductance symmetry throughout the entire irradiation period. Moreover, the sintered pellet manufacturing results found that excellent tolerances can be achievable for the annular pellet dimensions, which suggests that the rod can be assembled with a smaller inner gap and the ZrO_2 layer sputtering may not be needed. This needs to be further confirmed by irradiation, hence the ZrO_2 sputtering technique can serve as a backup.

To simulate fuel performance of VIPAC fuel, the FRAPCON3-ANNULAR(V) code was developed incorporating several new models. These include a new empirical VIPAC thermal conductivity relation as a function of temperature, burnup, porosity and gas pressures; a VIPAC fuel and cladding thermal conductance model based on the original FRAPCON thermal conductance model; a VIPAC fuel-cladding mechanical interaction model and a model of dimensional changes; and an athermal fission gas release model for VIPAC. The performance of the VIPAC fuel was compared to that of the sintered fuel. The results showed that during the first ~50 EFPDs, the VIPAC annular fuel average temperatures were lower than those of the sintered annular fuel in spite of the smaller conductivity of VIPAC fuel, a consequence of the absence of fuel cladding gaps. After the sintered annular fuel gaps close, the fuel average temperature drops below that of VIPAC fuel because of the higher thermal conductivity of sintered fuel. The VIPAC fuel fission gas is predicted to be higher than the sintered fuel (~12% at EOL for VIPAC versus ~6% for sintered fuel) because of the larger surface to volume ratio of VIPAC fuel particles, which facilitates escape of fission atoms by recoil and knock-out. On the other

hand, VIPAC fuel rod average cladding strains at BOL (when the strains reach maximum values) are substantially lower than those of sintered fuel, because the cladding creep-down during the initial operating period due to external pressure is alleviated. Also, the FRAPCON-ANNULAR(V) results confirmed the expectation of excellent gap conductance symmetry. Design optimization study of annular VIPAC fuel was also performed using FRAPCON3-ANNULAR(V) code. The optimum initial helium (fill gas) pressure was identified to be between 1.4 and 2.0 MPa, the optimum particle size yielding the lowest fission gas release was found to lie between 300-600 μ m, and the optimum smear density with respect to low fission gas release, low EOL rod pressure and small cladding strains was identified to be in the range of 85%-90%. Fuel performance models and the results are discussed in Chapter 13.

Regarding the irradiation tests of VIPAC fuel, only limited irradiation experiments could be performed because of time and funding constraints of this project. Since there is much more extensive experience with behavior of the sintered pellets than with VIPAC fuel, only VIPAC fuel was selected for irradiation tests. The goal of the irradiation experiment was to obtain data on fission gas release and potential irradiation induced dimensional changes, which could occur early during irradiation (note that no temperature driven sintering is expected due to very low fuel temperatures).

Two of the VIPAC annular fuel samples manufactured by AECL shown in Figure ES-4 were instrumented with thermocouples and assembled into irradiation capsules. Reactor coolant at 50°C was used for cooling. This required the design of special irradiation capsules that provide additional heat transfer resistance between the coolant and cladding to match the cladding and fuel temperatures to those predicted during PWR operation. This has been accomplished through a lead-bismuth eutectic filled gap. The seal welded and pressure tested capsules were inserted into the MITR-II core, as shown in Figure ES-6. The two assembled capsules were irradiated to a burnup of 6.9 and 5.7 MWd/kg-U over the period from March to September 2004. Linear heat generation rates in the fuel samples were near those that would be experienced in a peak rod at 150 percent of current PWR power density. After the irradiation was stopped, the fuel samples were transferred from the core to the fuel storage ring in the reactor for radiation decay before post irradiation examination (PIE).

The PIE, which was performed in August 2005, confirmed that predicted burnups were within several percent and showed lower than predicted fission gas release. Fission gas release was evaluated by comparing the integrated counts for the Kr-85 514 keV peak in the fuel to the integrated counts in the plenum region, and assuming that all released fission product gas has migrated to the plenum. About 0.5% fission gas release was estimated for each capsule. These values are subject to fairly large uncertainty because the 514 keV peak is close to the 511 keV annihilation peak and is not fully resolved from it. However, it is very likely that the actual fission gas release is not more than several per cent of the fission gas generated.

The details of irradiation program and the results are summarized in Chapter 14.

Figure ES-6 Top view of MITR core with loaded annular fuel

1. MOTIVATION

Electricity from nuclear fuel is currently generated, in most of the world as well as in the US, in pressurized water reactors (PWRs) or Boiling Water Reactors (BWRs). From the beginning of the nuclear era, development of these light water reactors (LWRs) has sought to maximize reliability and safety. Remarkable progress in safety has been achieved in the last three decades, often at the expense of increased cost, as new safety systems have been added to the nuclear island, increasing the plant complexity and thus both the capital and operating costs. On the other hand, the increased reliability of the plants has led to reduced costs of plant operation. The last few years have marked a period of rethinking of nuclear reactor technology because of a changing economic environment reflecting stronger competition with other power sources coupled with a deregulation trend in the electric utility industry. The new and highly competitive environment put all nuclear plants under increased pressure to significantly reduce total power cost. Therefore, reduction of total generation cost while maintaining excellent safety is a key challenge facing the nuclear industry today and in the future.

Even though no completely new nuclear power plant has been built in the U.S. for almost twenty years, there has been a substantial increase in the amount of power generated by the 103 existing nuclear reactor fleet as a result of significant improvements in capacity factors, completion of partially built plants and also of power uprates for existing plants. Power uprates of operating plants are attractive to utilities as they allow an increase of revenue without a large capital investment. Since many U.S. plants have applied for, and more than 36 have received license extensions, further uprates could supply significant electricity at modest investments. However, power uprates performed to date have used up most of the overly conservative margins which were built into the early plants before sophisticated analyses became available.

One attractive approach to improve the economy of both the operating and new plants is to increase their power density and extract more energy from a given system volume. Since nuclear power costs are mostly (about 65%) due to cost of capital, using the same volume of the nuclear island to extract more energy is the best way to reduce the total cost of nuclear power. One of the key components affecting the allowable power density in the nuclear island is nuclear fuel. In fact, the safety limits in a nuclear power plant are largely related to its fuel. This has been recognized from the early days of nuclear technology and significant efforts have been devoted to achievement of excellent performance of current LWR fuel. Evolutionary improvements in fuel design and cladding quality allowed a remarkable reduction of failure rate, and fuel assembly design changes allowed both power increases and performance improvement at steady state and during accidents. A lot of improvement in performance relied principally on increasing the surface to volume of the fuel, through increasing the number of fuel rods per assembly, thus using smaller and smaller fuel pins. Therefore, incremental benefits have been realized, both from a safety perspective and from a plant economy perspective. However, the basic LWR fuel design in the form of cylindrical pins of about one centimeter in diameter with a fixed water-to-fuel volume ratio (of about 1.7 for PWR and 2.0 for a BWR) has remained unchanged since the time of its conception. But, today's PWRs use mostly 17x17 arrays or larger arrays instead of the 15x15 old ones. Similarly,

BWRs are moving from 8x8 to 9x9 and even to 10x10 arrays after a start with 6x6 arrays. However, there are limits to how small can the solid pins be while still maintaining enough rigidity to resist vibrations, and avoiding the penalty of corrosion of thinner cladding.

Given the increased interest in new power plants, it is timely that the fuel design be revisited. Using the extensive experience with UO₂ fuel, new geometric arrangements that allow substantial increases in power density should be developed. Several ideas are currently being explored at our Center for incorporating the LWR fuel in a new geometry, and even new materials, to permit a large increase of power output from the same core (and vessel) volume, thus decreasing the capital cost per installed kW. [Feng et al., 2005, Blair and Todreas, 2003]. The geometry has to satisfy all the normal safety requirements, and also be acceptable from a mechanical and manufacturing point of view. In this special issue, a summary of the investigation of the use of annular fuel, with internal and external cooling, in PWRs is given and contrasted to today's fuel. Application of this fuel to BWRs are being investigated as well, but have not yet reached the same level of maturity [Morra et. al., 2004].

2. HIGH POWER DENSITY ANNULAR FUEL AND KEY CONSTRAINTS FOR ITS DESIGN

Having recognized this potential, we proposed a new fuel geometry [Hejzlar et al., 2001], which would allow a substantial increase in power density on the order of 30% or higher, while maintaining or improving safety margins comparable to currently operating PWR plants. The fuel is of annular shape and has both internal and external cooling. To provide sufficient flow rate through the inner cooling channel, significantly larger rod size than the typical fuel rods of 17x17 PWR fuel arrays has to be employed. Therefore, for a fixed assembly size, the PWR fuel assembly has a smaller number of fuel rods.

A transition from solid to annular geometry has two important implications that allow higher power density: (1) reduction of the thickness of the heat conduction path, which improves the margin from peak fuel temperature to melting and (2) increased heat transfer surface area (in spite of a reduction of the number of fuel rods), which enlarges the Departure from Nucleate Boiling Ratio (DNBR) margin. In addition to peak fuel temperature and DNBR limits, the new fuel has to satisfy a number of other safety limits and performance constraints. Below are the key constraints that need to be considered together with the features of the new fuel that address these issues:

1. Satisfactory peak cladding temperature during loss of coolant accident (LOCA). Low operating temperature of annular fuel will reduce significantly stored energy and thus blowdown peak cladding temperature during LOCA.
2. Maintenance of key reactor physics parameters, in particular reactivity coefficients and shutdown margin comparable to those of current PWR. This will require preservation of heavy metal to moderator ratio close to the value used in current PWRs, which has been fairly well optimized.

3. High burnup capability. To preserve an 18 month fuel cycle length at higher power density, the new fuel must be able to withstand significantly higher burnup than current solid fuel. Since one of the key limitations on discharge burnup is fission gas release (FGR) that leads to fuel rod pressurization, much lower fuel operating temperature will be beneficial in reducing FGR and enabling higher burnups*.
4. Resistance to vibration. Substantial power uprate in PWRs needs to be accompanied by an appreciable increase of the core flow rate to maintain the outlet temperature below saturation. Higher flow rates can cause vibration of fuel rods and lead to damage. The proposed annular fuel has much a larger moment of inertia and is more rigid than solid rods, thus improving stability against vibrations.
5. Acceptable thermal and mechanical materials performance. Pellet-cladding mechanical interaction from pellet expansion and swelling, especially during transients could lead to cladding failure, hence uniform cladding (plastic and elastic) strain is limited to 1%. Cladding strain in annular fuel will be to some extent limited by additional gap which provides additional degree of freedom for the pellet movement.

Finally, it is important to note that the new fuel should be easy to manufacture and should not have significantly higher cost than current solid PWR fuel. While in principle a higher fuel manufacturing cost can be more than offset by the extra power it will enable in a plant, it is desirable to have the cost of the new fuel within a reasonable range of the cost of the old fuel to ensure total economic advantage.

3. BACKGROUND OF ANNULAR FUEL

The proposed annular fuel departs from the traditional solid rod design by introducing internal cooling of each fuel pellet. The idea of annular fuel pellets is not new in reactor technology, although it has been usually applied within the solid cylindrical cladding. In pressurized water reactors, annular fuel pellets are used in the short axial blankets of western PWRs and BWRs and in Russian VVER cores, in which pellets employ a small (~10 vol%) central voided region to reduce fuel centerline temperature. Annular fuel pellets were also proposed for axial power flattening without the need to vary enrichment by having annular pellets stacked in the fuel rod in such a way as to compensate for neutron flux variation along the core height through changes of the diameter of a central voided region in each pellet [Mildrum, 1987]. All these annular pellets, either employed in or proposed for PWRs considered only a central voided region without internal cooling, where the benefit of reduced fuel centerline temperature was marginal and the

* Significantly higher burnup will have benefit of reduced space requirements for interim storage and for repository as more energy is generated per fuel assembly.

more limiting minimum DNBR margin could not be appreciably affected. In fact, in detailed neutron physics and economic evaluations, annular pins with voids were not found to offer fuel cost or uranium utilization benefits at extended burnups [Mildrum, 1980].

Annular fuel with both internal and external cooling was proposed for high temperature gas cooled reactors where a compact fuel element of annular shape was conceived to be enclosed in an inner tube and an outer tube. The inner tube was made of graphite having a lower coefficient of shrinkage than the graphite of the outer tube under irradiation to attain good contact of both tubes at high fluences [Bujas, 1975]. Annular fuel has also been evaluated in the framework of the Strategic Defense Initiative (SDI) for a Wire Core Reactor proposed by Rockwell where Tungsten-Rhenium clad UN fuel wires, 0.05 to 0.25 cm in diameter, are shaped into a number of concentric annular fuel assemblies having alternating layers of fuel wires separated by tension-stressed unfueled spacer wires to maintain fuel element spacing and facilitate propellant flow [Marshall, 1989]. This, as well as a number of similar concepts, remained as proposals without actual application or operational experience.

Annular fuel cooled from both sides has been utilized in some research and special purpose reactors. For example, Atomic Energy of Canada Ltd (AECL) has recently developed an internally cooled oxide fuel element for use in the heavy water Maple X isotope production reactor in Canada. However, these “target” fuel elements operate at relatively low power density, and fuel endurance of only a few months is required. Also the dual purpose plutonium production N-reactor at Hanford site used 66cm-long tubular metallic uranium fuel that was placed in pressure tube coolant channels and effectively cooled from both the inside and outside. Although this reactor generated electricity, its primary mission was focused on plutonium production and was run at low power density and at small burnups. Most recently, Commissariat à l’Energie Atomique (CEA) proposed advanced fuel assemblies with large fertile-free annular fuel rods containing plutonium surrounded by traditional UO_2 solid rods [Porta et. al., 2001]. These fuel assemblies were evaluated for plutonium burning due to a softer local neutron spectrum and better capability of annular rods to accommodate higher power peaking. The annular fuel proposed here uses UO_2 fuel and is intended for all core assemblies with the prime objective being to significantly increase core power density, while increasing or at least maintaining safety margins.

4. OBJECTIVE

The overall objective of this NERI project is to develop and evaluate innovative internally and externally cooled fuel that will allow a significant increase of core power density while maintaining or improving safety margins so that the capital cost per installed kW as well as the operating and fuel cycle cost per unit energy produced are decreased. In addition, high fuel burnup is pursued utilizing the benefits of very low operating fuel temperatures to further improving fuel cycle economics, reduce total spent fuel production per unit energy, as well as increasing proliferation resistance of the spent fuel.

The new fuel design would accommodate a substantial increase of core power density while simultaneously providing comparable or larger thermal margins than current PWRs. The goals of this project were:

1. Identify the most promising fuel assembly arrangement for PWRs to achieve a significant increase in power density of at least 30 percent; based to a large extent on the extensive PWR UO_2 fuel database to minimize R&D development effort and the risk associated with transition to a new fuel material. Optimize the fuel for superior thermal hydraulic performance. Examine flow distribution, core pressure drop, departure from nucleate boiling ratio (DNBR), and resistance to parallel channel instabilities.
2. Perform safety analyses, such as loss of coolant accident (LOCA) analyses, to confirm safety benefits for the optimum configuration identified in item (1).
3. Evaluate the neutronic fuel design to achieve high reactivity-limited burn-up and a refueling cycle comparable to current PWR practice to attain good economic features. Confirm the acceptability of the coefficients for reactivity feedback and reactivity control. Examine the case of this fuel for plutonium burning in the future.
4. Select fabrication processes to produce annular fuel elements with the required product characteristics, including fissile loading and high integrity cladding, which are capable of eventual scale-up into efficient production processes for economic and reliable fuel element performance.
5. Evaluate the materials and mechanical performance of UO_2 fuel forms obtained by production technologies different from current U.S. practices (e.g., vibropacked fuel), and operating under new conditions (such as very low fuel temperature). Develop models for assessing fuel performance as well as for scoping irradiation tests performed at the research reactor of Massachusetts Institute of Technology (MIT).
6. Estimate the electricity cost in cases of using the annular fuel for uprating current Generation II PWRs or in new advanced PWRs.

5. REPORT ORGANIZATION

To achieve these goals, the project was organized into 5 tasks.

- Task 1 consisted of thermal hydraulic analyses including safety analyses (Goals 1 and 2). The results of this task are described in Chapters 6 and 7. Mechanical analysis with the focus on resistance to vibrations, was also addressed in this task. The results are described in Chapter 8.

- Task 2 involved neutronic evaluations (Goal 3) and the results are presented in Chapter 9. The feasibility and efficiency of using the higher power density to burn plutonium faster is examined in Chapter 10.
- Task 3 investigates issues and methods related to manufacturing of this advanced fuel (Goal 4), including new innovative fabrication processes to produce annular fuel elements with the required product characteristics. The outcome of this research is documented in Chapter 11.
- Task 4 evaluates fuel cycle cost and capital cost to determine the economic viability (Goal 5) of the high-performance fuel. The findings are summarized in Chapter 12.
- Task 5 analyzes fuel performance of the new UO_2 annular fuel obtained by various production technologies including irradiation testing in the MIT reactor. The results of these investigations are presented in Chapters 14 and 15.

6. THERMAL HYDRAULIC DESIGN

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6.1 Introduction and Objective

The new internally and externally cooled annular PWR fuel has been proposed [Hejzlar et. al., 2001] to substantially increase power density while retaining or improving safety margins. The geometry of this annular fuel is shown schematically in Figure 6.1, where the traditional solid fuel rod is also drawn for comparison. The annular fuel rods are of significantly larger diameter than the typical solid rods to accommodate an inner coolant channel allowing for sufficient coolant flow. Thus, for fixed fuel assembly dimensions, the number of annular fuel rods will be smaller than in the 17x17 array for solid pins, and the power per rod will be increased. This chapter describes the analysis of steady-state thermal-hydraulic performance that was carried out to identify the optimum geometry and the magnitude of the potential power density increase over the traditional PWR cores with solid rods.

Figure 6.1 Schematic of solid and internally and externally cooled annular fuel rod and associated coolant cell (not to scale)

The internally and externally cooled annular fuel rod has two cooling surfaces: the outer clad contacting the outer channel and the inner clad contacting the inner channel. The coolant from the lower plenum flows into both the outer channels which allows for mass, momentum and energy exchanges among adjacent channels, and also into inner channels, which are isolated and do not exchange mass and energy with other channels. The proposed annular fuel introduces a new variable that needs to be considered in the thermal hydraulic design – pin internal cooling flow rate. All current PWR cores employ fuel pins cooled at their external surface by an open flow, hence all the energy generated within the pin is transferred to the external flow stream among the fuel pins. For an annular fuel rod with an internal cooling hole, only part of the heat generated is transferred to the external coolant in the open fuel pin array, while the remaining part proceeds to the coolant flowing through the inner channel. An important task for the thermal-hydraulic optimization process is to achieve a coolant flow split between the outer and inner channels, and a heat conduction split in the annular fuel rods to the inner

and outer surface, such that the cladding surface temperature, coolant enthalpy rise, and especially DNBR in both channels are approximately equal. The MDNBR in the core is maximized when the MDNBR values in the inner channels and the outer channels are the same. An additional constraint in the design of annular fuel is the maintenance of heavy metal to moderator ratio close to that for current PWRs to retain the reactor physics characteristics.

The main objective of thermal-hydraulic design of the annular fuel is to identify the most promising fuel pin configuration and assembly design that achieves a significant increase in PWR power density while having comparable or better safety margins to current PWR fuel design. The analyses here are limited to steady state thermal hydraulic conditions.

6.2 Reference PWR Core and Parameters

To evaluate the performance of the internally and externally cooled annular fuel, it is useful to select a reference current design against which to compare the new fuel. A typical Westinghouse 3411 MWth four loop PWR plant has been selected as the base case for the study. Table 6.1 summarizes the design data of the reference PWR core. The data were provided by Duke Engineering and Services (formerly Yankee Atomic Electric Company) [McMahon et. al., 1997]. The details on the grid locations and grid type were not available. The L-grids with mixing vanes were selected for the base case calculations because the DNB correlation for this type of grids (W-3L) is available in the open literature. The grids are assumed to be spaced 20 inches apart, which is a typical spacing for these grids. The effective core flow rate for heat removal was taken as 5% less than the total flow rate to account for bypass flow. All values in Table 6.1 are for cold conditions.

6.3 Exploratory Study Using One-Rod Model

First an exploratory analysis of the externally and internally cooled annular fuel to identify the dimensions that will result in a flow split that produces the largest MDNBR was carried out using a simple one-rod model. The model consists of one fuel rod and two coolant channels - the inner channel that is inside the annular fuel rod and the outer channel that cools the outer cladding surface. The analysis was performed for the hot rod (with the largest power peaking factor) using the in-house computer code TAFIX (Thermal-Hydraulic Model of Annular Fuel with Internal and eXternal Cooling). The code calculates the coolant flow split between the inner and outer channels for a given total flow rate per fuel rod and from the equality of pressure drops. The fuel rod consists of three conducting regions with heat generation – inner cladding, fuel pellet, and outer cladding regions (each nodalized in the radial direction) and two gap regions, which are treated as massless conductors. Heat transfer from the annular fuel rod to the cladding surface in contact with coolant is computed by a numerical solution of the 1-D heat conduction equation in cylindrical coordinates using the approach from the RETRAN computer code [McFaden et. al., 1984]. The detailed description of the model is given in [Kazimi et. al., 2001].

Table 6.1 Operating parameters and selected characteristics of a typical Westinghouse 4-loop PWR [McMahon et. al., 1997]

Parameter	4-loop PWR
1. Plant	
Number of primary loops	4
Reactor thermal power (MWth)	3411
Total plant thermal efficiency(%)	34
Plant electrical output	1150
Power generated directly in coolant (%)	2.6
Power generated in the fuel (%)	97.4
2. Core	
Core barrel inside diameter/outside diameter (m)	3.76/3.87
Rated power density (kW/L)	104.5
Core volume (m ³)	32.6
Effective core flow area (m ²)	4.747
Active heat transfer surface area (m ²)	5546.3
Average heat flux (kW/m ²)	598.8
Design axial enthalpy rise peaking factor ($F_{\Delta h}$)	1.65
Allowable core total peaking factor (F_Q)	2.5
3. Primary Coolant	
System pressure (MPa)	15.51
Core inlet temperature (°C)	292.7
Average temperature rise in reactor (°C)	33.4
Total core flow rate (Mg/s)	18.63
Effective core flow rate for heat removal (Mg/s)	17.7
Average core inlet mass flux (kg/m ² -s)	3,729
4. Fuel Rods	
Total number	50,952
Fuel density (% of theoretical)	94
Fuel pellet diameter (mm)	8.19
Fuel rod diameter (mm)	9.5
Cladding thickness (mm)	0.57
Cladding material	Zircaloy-4
Active fuel height (m)	3.66
5. Fuel Assemblies	
Number of assemblies	193
Number of heated rods per assembly	264
Fuel rod pitch (mm)	12.6
Fuel assembly pitch (mm)	215
Number of grids per assembly	7
Fuel assembly effective flow area (m ²)	0.02458
Location of first spacer grid above beginning of heated length (m)	0.3048
Grid spacing (m)	0.508
Grid type	L-grid*
Number of control rod thimbles per assembly	24
Number of instrument tubes	1
Guide tube outer diameter (mm)	12.243
6. Rod Cluster Control Assemblies	
Neutron absorbing material	Ag-In-Cd
Cladding material	Type 304 SS
Cladding thickness (mm)	0.46
Number of clusters Full/Part length	53/8
Number of absorber rods per cluster	24

*Employs mixing vanes

6.3.1 Search for Optimum Thermal Hydraulic Performance

To identify the optimum dimensions of the annular fuel pin and the array size applicable to the reference PWR core, the overall dimensions of the new annular fuel assembly were assumed fixed equal to those of the reference 17x17 bundle with solid pins. A range of array sizes from 15x15 to 11x11 were analyzed. The pin size was increased with decreasing array size. Thus, the pin linear heat rate and coolant flow rate per fuel rod was increased, for fixed total power.

In order to perform this exploratory analysis of the design of the annular fuel assembly, the following assumptions were adopted:

- Power peaking for the hot rod in the core is the same as for the reference Westinghouse PWR core (2.5 total, axial distribution – chopped cosine with peaking of 1.55). Analyses were performed at 18% overpower, to allow for transients.
- Mass flow rate per fuel rod was taken equal to core-average flow rate per rod.
- Thickness of both inner and outer cladding is identical and equal to the cladding thickness of a solid pin in the Westinghouse 17x17 array (this assumption was supported by the results from the fuel and cladding performance work discussed in Chapter 13).
- A pellet fuel is employed with symmetrical gap conductance of $6000 \text{ W/m}^2\text{-K}$ on each side.
- Form loss coefficients on grids used in the calculations were assumed 0.6 for 11x11 through 13x13, 0.7 for 14x14 and 1.0 for 15x15 arrays. Note that grids with higher form loss for smaller fuel elements are necessary to force sufficient flow into the inner channel. However this will apparently increase the total core pressure drop.
- Because the added inner cladding will take up some volume in the core, the coolant volume and fuel volume will be smaller than those in the reference core. The fuel dimensions (internal diameter) were selected in such a manner to obtain at least 90% of the fuel volume in the reference 17x17 design and about the same fuel to moderator volume ratio.
- A constraint of minimum rod-to-rod gap of 1mm is adopted. This is smaller than the 3mm-gap in the reference 17x17 fuel; but is feasible based on manufacturing, vibration and pressure drop considerations. The larger annular rods are stiffer and therefore less prone to vibrations than the smaller solid rods, providing the possibility to employ a smaller gap, as long as the pressure drop remains reasonable [Zhao et al., 2004].
- The number and layout of guide tubes for the 15x15 case was adopted from an earlier 15x15 Westinghouse designs. For smaller array sizes, smaller numbers of

guide tubes were chosen since they can contain larger control rods and fewer larger guide tubes are needed to hold the assembly together.

Table 6.2 Dimensions (cm) of annular fuel elements of various arrays

Array	Dcii	Dcio	Dfi	Dfo	Dci	Dco	Pitch
11x11	1.0733	1.1876	1.20	1.700	1.7124	1.8267	1.952
12x12	0.9533	1.0676	1.08	1.540	1.5524	1.6667	1.789
13x13	0.8633	0.9776	0.99	1.410	1.4224	1.5367	1.651
14x14	0.7533	0.8676	0.88	1.294	1.3064	1.4207	1.533
15x15	0.6733	0.7876	0.8	1.1978	1.2102	1.3245	1.431
17x17-ref.	Solid pin	-	-	0.8255	0.8379	0.9522	1.263

Based on the above assumptions and restrictions, typical dimensions of fuel rods for each array size are listed in Table 6.2, where in the first subscript c and f stand for cladding and fuel, respectively; in the second subscript, i and o designate inner cladding and outer cladding, respectively, or inner diameter and outer diameter for the fuel ring; and the third subscript denotes the diameters of the cladding (i=inner, o=outer).

The dimensions of fuel rods for each array size shown in Table 6.2 are typical dimensions, which means the dimensions could be a little different from those shown in the table, but there is little room for them to change mainly resulting from the adoption of the restrictions of 1mm minimum rod-to-rod gap, at least 90% fuel volume to the reference and fuel volume to coolant volume ratio. Fine adjustment of the rod dimensions for a promising assembly size will be discussed in Section 6.5 using whole core analysis. The dimensions of the reference solid fuel pin are also shown. The layout of the fuel rods and guide tubes for 13x13 through 15x15 arrays is shown to scale in Figures 6.2 through 6.4.

Figure 6.5 plots selected key parameters characterizing important neutronics and thermal hydraulic performance as a function of array size. It can be observed that the total cooling surface is significantly higher than that for the solid fuel and becomes smaller with decreasing array size. Fuel and coolant volumes and fuel-to-moderator ratio are kept close to the value for the reference 17x17 assembly.

The desirable key thermal hydraulic figures of merit are:

- Low peak fuel temperature,
- Low core pressure drop, and
- High MDNBR.

Figure 6.6 plots radial temperature profiles at the location of the peak linear heat rate of fuel elements for each array size. As the number of fuel rods in the assembly decreases, the rod linear heat rate is increased for fixed assembly power, which results in a higher peak temperature. The higher peak temperature results from the thicker fuel rods of smaller assembly sizes. Reducing the number of rods results in a more pronounced peak fuel temperatures, albeit the values are still well below those for the solid fuel case.

Figure 6.2 Fuel assembly in a 13x13 array

Figure 6.3 Fuel assembly in a 14x14 array

Figure 6.4 Fuel assembly in a 15x15 array

Figure 6.5 Key parameters affecting neutronics and thermal hydraulics compared to 17x17 reference solid Fuel

Figure 6.6 Radial fuel temperature profiles for various array sizes

MDNBR in both the inner and outer channel is plotted as a function of array size in Figure 6.7. For arrays with a larger number of fuel rods, the MDNBR occurs in the inner channel. As the number of fuel rods is reduced, the diameter of the inner channel is increased allowing for more flow and the MDNBR shifts to the outer channel. Clearly, there is an optimum with the maximum DNBR margin for the 13x13 array. Moreover, the MDNBR for this array is well balanced, i.e., it is about the same in both the inner and outer channels.

Finally, core pressure drop at 100% power is plotted in Figure 6.8. The pressure drop increases with the number of rods in the assembly. This is due to the smaller hydraulic diameter of the coolant channels. In addition, forcing more flow into the small inner channel in an effort to improve DNBR margin, requires increasing form losses in the outer channel by modifying the grids. This contributes to a steeper rise of pressure drop for assembly sizes 14x14 and higher. Because of the large increase of pressure drop for array sizes beyond 13x13 and because the 13x13 array has pressure drop close to that of the reference 17x17 PWR fuel, the 13x13 array appears to be also the suitable choice from the pressure drop viewpoint.

Figure 6.7 Minimum DNBR as a function of array sizes

Figure 6.8 Core pressure drop for various array sizes

Based on this simplified one rod thermal hydraulic analysis, the optimum configuration for the annular fuel appears to be a 13x13 array. It offers the highest DNBR

margin, small peak fuel temperature and relatively small pressure drop. Therefore the 13x13 design with dimensions from Table 6.2, designated PQN-02, was selected as the reference design to be subjected to further optimization incorporating neutronics, fuel performance, and mechanical design aspects.

6.4 Performance of the 13x13 Annular Fuel Design

6.4.1 Key Benefits of the 13x13 Annular Fuel

Figure 6.9 compares fuel temperature profiles at the hot spot for the solid and internally cooled annular fuel. It shows that for fixed power density the hot spot of the annular fuel rod in a 13x13 fuel assembly has a 1500°C lower peak fuel temperature than a typical solid fuel rod of the Westinghouse 17x17 design. It is also shown that a substantial increase in pin power (by 50%) is possible while peak fuel temperature remains 1100°C less than in the reference case. This key advantage stems from two factors: (1) the fuel thickness is reduced to about one half of its original value and (2) introduction of double sided cooling further renders the effective conduction thickness to almost 25% of its original value. Moreover, lower temperatures bring additional temperature reduction, since the thermal conductivity of UO_2 is somewhat higher at lower temperatures.

Figure 6.9 Comparison of fuel temperatures at the hot spot

The first significant benefit of such a low temperature is small stored energy and thus significant increase of the grace period before emergency coolant needs to be delivered in a LOCA, or substantial reduction of peak cladding temperature during the blowdown

phase. Adiabatic heat up calculations, where at zero time the heat transfer to coolant is set to zero and power is reduced to 6.5% (decay heat level), show that for a fixed core power, the time before the cladding temperature reaches the 1200°C limit, is increased by an order of magnitude (in comparison with solid fuel) [Kazimi, et al, 2002].

Another benefit of low fuel temperature is much smaller fission gas release, which results in better fuel performance during irradiation, as discussed in Chapter 13. Low-temperature fuel has also less propensity to balloon during core overheating accidents such as LOCAs. In addition, much smaller fuel temperatures and temperature gradients are expected to produce less pellet cracking and thus may reduce pellet cladding interaction (PCI).

Significant increase of the cooling surface results in an increase of DNBR margin for fixed core power. This can be observed from Figure 6.7, which shows MDNBR of the 13x13 array to be close to 3, which is almost twice as large as for solid fuel at the same boundary conditions. The larger MDNBR was mainly a result of its larger fuel surface cooling area and this makes the annular fuel an attractive option for power density uprate.

The last main advantage of the annular fuel is its large rigidity because of its tube-like geometry with large diameter, which has high moment of inertia. This makes the annular fuel more resistant to flow induced vibration than the reference solid fuel.

6.4.2 Preliminary Estimate of Possible Power Uprate with the 13x13 Annular Fuel Assembly

The advantage low fuel temperature provides the annular fuel a large room for power uprating before meeting the limitation of fuel temperature and stored energy in fuel for a benign cladding behavior during transients. Also the annular fuel core has about 50% percent more cooling surface than the reference solid fuel design, it can be roughly estimated that the annular fuel core can operate at 150% reference power and has the same DNBR if the coolant condition, i.e. critical heat flux of the coolant is kept the same.¹

Further consideration should be given to core flow rate and core pressure drop. Power output extracted from a PWR core is proportional to coolant mass flow rate and core temperature rise. If the core outlet temperature is to be maintained below saturation and core inlet temperature remains unaltered to maintain plant efficiency, the flow rate needs to be increased proportionally to power. Thus, a major limiting thermal hydraulic parameter is the pressure drop across the core. It can be observed from Figure 6.10 that velocities in the both inner and outer channel increase linearly with increasing power (and thus flow rate) and pressure drop grows with power squared. In the reference PWR core, average velocity is about 5m/s. As a rule of thumb, velocity in water cooled systems should not exceed 10m/s. However, mechanical design and vibration-related issues will limit velocity to lower values.

¹ For power uprating through proportionally increasing the core flow rate, CHF of the coolant will be increased because of the benefit from increased mass flux.

Flow-induced vibrations in nuclear reactor components are generally due to four mechanisms: (1) fluid elastic instability, (2) periodic vortex shedding, (3) turbulence induced excitation, and (4) acoustic resonances. The analysis of annular fuel at 150% flow rate and power showed that it is more resistant to all these types of vibration than solid fuel at 100% power [Zhao et al., 2004]. Therefore, to keep coolant velocity and pumping power at reasonable levels, a 50% power density increase was adopted as the target power level. Implications of this power uprate for the MDNBR margin, will be evaluated later in a more detailed whole-core VIPRE model.

Figure 6.10 Required velocity and core pressure drop to maintain the same power to flow ratio

6.5 Whole-Core Modeling of Annular fuel

6.5.1 Modeling approach and comparison with TAFIX

The single pin analysis served for fast scoping of arrays to identify approximately the most promising geometry of annular fuel rod. Since, it does not account for differences in flow rate distribution between the channels of various powers (or radial flow distribution in the core), a more sophisticated code is needed for more accurate analysis of this annular fuel. VIPRE-01/Mod2 (Versatile Internals and Component Program for Reactors; EPRI) [Cuta et. al., 1985] was selected for this purpose. It is a powerful code for thermal-hydraulic analysis of LWR cores extensively used by nuclear power utilities and certified by NRC. The code was developed for solid fuel rods, but can also calculate heat conduction in hollow tubes. This option of VIPRE-01 was used for modeling of annular

fuel rods with internal and external cooling. The fuel rods are defined as hollow tubes having several material regions with given radial and axial power factors. First, a VIPRE-01 model of one annular fuel rod was developed using the hollow tube option to compare its results to the TAFIX code.

In this model, the 13x13 annular rod is defined by five regions - inner cladding, inner gap, fuel meat, outer gap and outer cladding. Because the hollow tube option does not allow for heat transfer across a gap, the gaps have to be transformed to conductors having an effective thermal conductivity that matches the thermal resistance (or conductance) of the gap. For a gap having outside and inside radii R_{go} and R_{gi} , respectively and conductance h_g based on radius R_g , an effective thermal conductivity, k_g , can be obtained from the expression:

$$\frac{1}{2\pi R_g h_g} = \frac{1}{2\pi k_g} \ln \left(\frac{R_{go}}{R_{gi}} \right) \quad (6-1)$$

as

$$k_g = h_g R_g \ln \left(\frac{R_{go}}{R_{gi}} \right). \quad (6-2)$$

A benchmark calculation was performed for the 13x13 fuel rod design using the operating conditions in Table 6.3. The benchmark case calculation was performed for the hot rod (radial power peaking factor of the hot rod of 1.65) with an axially-average linear heat generation rate of 74.3kW/m, operating at 150% reference power and flow rate. The core-average coolant flow rate per one annular fuel rod is applied consistently with the approach used in TAFIX. Note that this is only a rough approximation of the flow rate since it does not capture flow rate reduction through the hot channel due to increased pressure drop from boiling, but valid for the purpose of this benchmark calculation between the two codes as long as the operating conditions for both codes are consistent.

Table 6.3 Operating Conditions for the Benchmark Calculation

Internally Cooled Annular Pin Type	13x13 fuel rod
Power	150% ($q'_{avg}=74.3\text{kW/m}$)
Coolant Flow Rate	0.78357kg/s (150% nominal flow rate)
Operating Pressure	15.50MPa
Coolant Inlet Temp.	567.87 K
Gap Conductance	6000W/m ² -K (for both gaps)
Axial Power Profile	Chopped Cosine (peak/average=1.55)

In both codes, the Westinghouse W-3S correlation without grid mixing factor is used to calculate DNBR in the inner channel because the inner channel does not have any grid, and Westinghouse W-3L correlation is used to calculate DNBR in the outer channel.

Table 6.4 compares the calculated pressure drops and the flow split. The agreement is very good with the differences being less than 3%.

Table 6.4 Comparison of calculated flow rates and pressure drops

	TAFIX		VIPRE-01	
	Inner Chan.	Outer Chan.	Inner Chan.	Outer Chan.
Flow Rate (kg/s)	0.404	0.380	0.415	0.370
Pressure Drop (kPa)	214.8	214.8	209.4	209.4

Comparison of DNBR values along the inner channel shows excellent agreement (see Figure 6.10). In the outer channel the DNBR values calculated by VIPRE-01 are about 5% lower than the TAFIX values, as shown in Figure 6.11. This is the consequence of a slightly higher quality along the entire height of the outer channel, which occurs due to somewhat smaller mass flow rate in this channel. VIPRE-01 quality near the channel entrance is above that of the TAFIX. This is due to a smaller VIPRE pressure at the channel inlet. VIPRE calculates the pressure inlet distribution from a given outlet pressure and its pressure drop is calculated from smooth wall correlations, while TAFIX considers roughness effects (roughness of 0.01mm was used).

Overall, it can be stated that TAFIX and VPRE-01 DNBR results are in very close agreement. Other thermal hydraulic parameters were also compared and showed good agreement within 6%. The small differences are due to different pressure drop and heat transfer correlations, different two-phase flow models and different steam water properties used in each code. Although VIPRE-01 does not provide the option of modeling fuel with internal cooling, the excellent agreement confirms that VIPRE-01 hollow tube option can be used with confidence to model internally and externally cooled fuel for the purpose of the determination of the MDNBR, provided that the fuel conductivity and pellet-cladding gap conductances are known.

6.5.2 VIPRE-01 Whole Core Model with Annular Fuel

The exploratory studies in Section 6.4 used a one-rod model consisting of one annular fuel rod with inner and outer channels for MDNBR analyses. These analyses identified the 13x13 array size to have the most promising thermal hydraulic performance. To obtain more realistic and accurate MDNBR, a whole core model is necessary. In particular, the major concern is correct representation of channel flow rates. The earlier models used the core-average mass flux, which does not account for flow rate reduction in the hot channels due to increased pressure drop in these channels as a result of increased subcooled, or possibly, saturated boiling. Therefore, it is expected that the MDNBR obtained from a full core model will be smaller than the values obtained from a one-rod model.

Figure 6.10 Comparison of DNBR in the Inner Channel

Figure 6.11 Comparison of DNBR in the Outer Channel

The 13x13 design was first chosen for the whole core modeling. In a whole core model, all rods and channels will be modeled and a sub-channel analysis scheme will be used. The power distribution in the hot assembly of the 13x13 configuration was calculated by MCNP [Kazimi et. al., 2002]. Radial peaking of the hot assembly was taken to be 1.587 (this includes 6% peaking increase to account for uncertainties and it is the same peaking as for the calculations of a conventional solid fuel PWR core). The power distribution in the hot assembly can be established as shown in Figure 6.12. In the same manner as for the reference PWR calculations, the hot assembly was moved to the center of the core and surrounded by assemblies with the same power. This will minimize the effects of mixing among the outer channels in these assemblies and will yield conservative DNBR results. Powers of the fuel assemblies of the core periphery were adjusted to maintain correct normalization. Figure 6.13 shows the assembly power distribution in the core. A whole core VIPRE-01 model for the 13x13 annular fuel was developed based on the power distribution and numbering schemes according to Figures 6.14 and 6.15. Note that the sub-channels around the hot rod are treated as individual channels while those several pitch lengths away from the hot rod or the hot sub-channel are lumped gradually into larger size channels.

Because VIPRE-01 does not provide an option for fuel rods with internal cooling, the annular fuel rods were specified as heat generating tubes with five material regions, as discussed in the one-rod model section. The annular rods are internally and externally cooled by the coolant in the inner and outer channels, respectively. Flow in the inner channels does not have mass and energy exchange with other channels, while flow in the outer channels exchanges both mass and energy with adjacent outer channels through interface regions (called gaps).

Figure 6.12 Pin power distribution in the hot fuel assembly with 1/8 symmetry

Figure 6.13 Assumed assembly power distribution in the 1/8th core

Figure 6.14 Numbering scheme of channels and rods in the hot assembly

Details of the whole core model are shown in Table 6.5. Two key parameters considered in VIPRE-01 code influencing lateral heat and mass exchange among the outer channels considered in VIPRE-01 code are turbulent mixing coefficient and resistance to lateral flow.

$$w' = \beta_s \bar{G} \quad (6-3)$$

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calculations [Rossi, 1986]. The W-3L correlation, i.e., the same as in one-rod model, was used for MDNBR calculations.

Table 6.5 Summary of the VIPRE-01 whole core model with 13x13 annular fuel

Parameter	Specification
Model	Whole core model with lumped sub-channels
Model region	1/8 core, full axial length
Fuel rod outer diameter	1.5367 cm
Fuel rod inner diameter	0.8633 cm
Guide tube outer diameter	1.5367 cm
Guide tube inner diameter	1.40 cm
Pitch	1.651 cm
Number of channels	49 channels – see Figure 6.12 & 6.13
Number of rods	23 rods –see Figure 6.12 & 6.13
Axial power profile	Chopped cosine, peak-to-average ratio = 1.55
Radial power distribution	See Figure 6.14 & 6.15
Reactor power	150% reference power at 118% over power (6037.5MWth)
Power deposited directly in coolant	0.0%
Core mass flow rate	150% reference flow rate reduced by 5% (26,550kg/s)
Core inlet temperature	Reference temperature increased by 2°C (294.7°C)
Cross flow resistance coefficient	$K_G = 7.33 Re^{-0.2}$
Turbulent mixing model	$\beta = 0.0$
Turbulent momentum factor	$FTM = 0$
Axial friction coefficient for turbulent flow	$f_{ax} = 0.32 Re^{-0.25}$
Form loss coefficient for mixing vane grids in outer channels	0.6
Inlet and outlet form loss coefficient	0.4 for inlet and 1.0 for outlet
CHF correlations for inner channels	W-3S, grid mixing factor 0.0
CHF correlations for outer channels	W-3L, grid mixing factor 0.043, grid spacing factor 0.066, grid factor leading coefficient 0.986
Void correlation	EPRI void model for subcooled void, Zuber-Findlay drift flux equation with coefficients developed for the EPRI void model for bulk void/quality correlation, Columbia/EPRI correlation for two-phase friction multiplier
Heat transfer correlation	Dittus-Boelter for single-phase flow, Thom correlation plus single-phase correlation for subcooled and saturated nuclear boiling

For the cross flow between adjacent channels, the gap width, centroid length and cross flow resistance coefficient K_G , defining the cross flow pressure drop,

$$\Delta p_{cross} = K_G \frac{|w| w v'}{2s^2}, \quad (6-4)$$

need to be supplied. In Equation (6-4), w is the cross flow through the gap in kg/m-s, v' is specific volume for momentum in kg/m³ and s the gap width in m. A typical value for the flow resistance coefficient between the two rods is on the order of 0.5. This value is usually used in the subchannel analysis, since for the predominant axial flows the crossflow resistance has insignificant effect on the mass flux and DNBR [Cuta et. al., 1985]. More exact values can be obtained from a Blasius-type relation. For the cross flow across the tube bundle on a square pitch, an Idelchik [1993] diagram can be used to derive for the pitch of 16.51mm and rod diameter of 15.37mm

$$K_G = 7.33 Re^{-0.2}, \quad (6-5)$$

where Re is based on lateral velocity and rod diameter. This relation was used in the VIPRE-01 whole core model.

The turbulent momentum factor, FTM, which determines the degree to which the turbulent crossflow mixes momentum, can be specified on a scale from 0.0 to 1.0, where zero implies that turbulent cross flow mixes only enthalpy and 1.0 specifies that momentum is mixed to the same extent as enthalpy. An FTM of 0.0 was conservatively chosen for the present model. Note that VIPRE results are insensitive to the choice of FTM.

To prevent bypass, flow into the guide tubes has to be restricted. In practical design, it is easy to limit the flow through guide tubes by inserting flow restrictors on guide tube inlets. Thus, in the whole core model, flow through guide tubes was fully blocked, i.e., subchannels in the guide tubes were not modeled, while the total core flow rate was reduced by 5% in order to address the by-pass flow. The same flow reduction was used for the whole core model of the reference solid fuel.

The MDNBR and core pressure drop results for the 13x13 annular fuel at 150% power are summarized in Table 6.6. The MDNBR in the inner channels and outer the channels are 1.370 and 1.932 respectively. It is noted that while MDNBR in both the inner and outer channel remain above 1.3 limit, the MDNBR in the inner channel is significantly smaller than in the case of one-rod model. Reasons for this difference are discussed next.

Table 6.6 Whole-core MDNBR and total pressure drop results

	MDNBR in inner channels	MDNBR in outer channels	Total pressure drop (MPa)
13x13 annular fuel Design (PQN-02)	1.370	1.932	0.243

6.5.3 Comparison of Whole Core Model and the One-Rod Isolated Channel Model

The MDNBR and pressure drop results of the whole core model are compared with those of the one-rod isolated channel model in Table 6.7. It can be observed that the isolated channel model yields larger total pressure drop and larger MDNBR value in the inner channel than the whole core model.

Table 6.7 Comparison of Whole Core Model with One Isolated Channel Model of 13x13 annular fuel design

	MDNBR in inner channels	MDNBR in outer channels	Total pressure drop (MPa)
Whole core model	1.370	1.932	0.243
One-rod model	2.280	1.714	0.288

This difference is primarily the consequence of the inaccurate estimate of the flow rate (core-average value was used) for the hot annular rod in the isolated rod model. Clearly, this approach overestimates the flow rate through the hot channel because in a core the driving pressure drop in the hot channel is determined by core-average channels, which do not experience subcooled boiling, while the hot channel has higher pressure drop due to boiling along significant section of the channel. Thus, the mass flux in the hot channels is lower than in the average channels. Figure 6.16 compares the mass flux along the hot inner and outer channels of these two models. In the isolated channel model, the mass flow flux in both the inner and outer channels is constant along the flow direction. Because the outer channel employs seven grids and the hydraulic diameter of the inner channel is larger than that of the outer channel, the mass flux in the inner channel is significantly higher than that in the outer channel.

Figure 6.16 Mass flux in hot inner and outer channels calculated by different models

The whole core model predicts a smaller total pressure drop, 0.243 MPa, compared to the isolated model, 0.288 MPa – see Figure 6.17. In the inner channel, the mass flux calculated by the whole core model, was smaller than that calculated by the isolated channel model. Although the flow conditions in the inner channel calculated by these two models were different, the heat fluxes to the inner channel are practically unaffected, as shown in Figure 6.18. This is because the difference between the flow conditions calculated by these two models is relatively small and the total thermal resistance between the fuel meat and the coolant is not affected much by the heat transfer coefficient at the rod surface. Thus, given the same heat flux from both models and the differences in the mass fluxes, the coolant temperature in the inner channel calculated by the whole core model is higher than that calculated by the isolated model (see Figure 6.19). Correspondingly, DNBR in the inner channel, calculated by the W3-s correlation using the whole core model is smaller than that calculated by the isolated model (see Figure 6.20).

Figure 6.16 shows that the axial mass flux in the hot outer channel calculated by the whole core model decreases along the channel height and ends up below the values obtained from the one-rod model in the upper half of the assembly. In the whole core model, the hot outer channel is open and it exchanges mass, energy and momentum with adjacent sub-channels through the interfacing gaps. VIPRE iterates at every axial node the cross flow and solves the mass, energy and momentum equations till the pressure gradient between adjacent sub-channels equals the cross flow resistance. At the lower half of the core, boiling does not occur and the axial flow resistances in the hot and cold outer channels are almost the same. Thus, the mass flux in the hot outer channel is almost the same as that in the core-average channels and its value is larger than that calculated by the one-rod model, the constant mass flux of which is determined by its total pressure drop. At the upper half of the core, subcooled boiling occurs in the hot channels and resistances to axial flow become larger. Therefore, the flow in the hot outer channel is forced by pressure gradients to colder adjacent channels, resulting in a gradual reduction of the hot outer channel flow. Although the mass flux in the upper half of the hot outer channel is smaller than that calculated by the one-rod model and heat fluxes to the outer channel calculated by both models are the same, the coolant temperature calculated by the whole core model is smaller than that of the one-rod model. This is the benefit of (i) the larger coolant flow rate in the lower half core that keeps the coolant temperature lower and (ii) coolant mixing in the outer channels in the whole core model. As a result, the DNBR in the hot outer channels calculated by the whole core model is larger than that calculated by the one-rod model.

Figure 6.17 Pressure drop (from outlet) in the hot inner and outer channels calculated by different models

Figure 6.18 Heat flux to the inner and outer channels calculated by different models

Figure 6.19 Coolant temperatures in the hot inner and outer channels calculated by different models

Figure 6.20 DNBR in the inner and outer channels calculated by different models

In conclusion, comparing the whole core model and the one-rod model, the one-rod model gives higher MDNBR results in the hot inner channel because of higher flow rate through the hot inner channel. On the other hand, the one-rod model gives smaller MDNBR in the hot outer channel because of a smaller flow rate at lower half of the fuel channel than the full core model, which results in a higher coolant enthalpy entering the upper half of the fuel channel, and thus in lower MDNBR. This occurs in spite of the fact that the total pressure drop in the hot outer channel of the one-rod model is larger than that in the hot outer channel of the whole core model.

The MDNBR in the inner channel is more sensitive to radial peaking than in the outer channel because of lack of cross flow. It is noted that the results in the inner channel of the one-rod model could be improved if the model is run twice – first for the core-average channel to obtain the driving pressure drop, which can then be used as a boundary condition for a one-rod model to determine the flow rate through the hot inner channel. Since the pressure drop obtained in this manner will be lower the flow rate through outer channel will be reduced resulting in an even smaller MDNBR (i.e., further from the results of the whole core mode) in the outer channel of the one-rod model. Therefore, the whole core model is necessary for realistic MDNBR predictions and it was used as the main tool for MDNBR analysis of annular fueled cores.

6.6 Optimization Study Using VIPRE-01 Whole Core Model

Because of the appreciable differences between the results calculated by the one-rod and whole core models, the search for the optimum geometry was partially repeated using the more rigorous whole core model. This has been performed for the assembly sizes of 12x12 to 14x14 using the VIPRE-01 whole core model. First, the optimum array size was identified. Then the rod dimensions were fine tuned to achieve the best performance in terms of MDNBR margin.

6.6.1 Search for the Best Assembly Size

Whole core calculations for the 12x12 and 14x14 assembly sizes were performed for the same hot rod power peaking within the hot assembly and core-wide power peaking as for the 13x13 array (see Figure 6.13). The annular rod dimensions used in the analysis were the same as those given in Table 6.2.

The MDNBR results for the above three assembly sizes are shown in Figure 6.21. The 14x14 assembly size could not satisfy the MDNBR limit at 150% reference power density because of insufficient flow rate through the hot inner channel. This problem can not be resolved through fine-tuning of the fuel dimensions [Feng, 2005], unless grids with high loss coefficient are implemented to force more flow through the inner channel, resulting in an unacceptable pressure drop. The array sizes of 12x12 and 13x13 can both satisfy the 1.30 MDNBR limit at 150% power density. Moreover, the MDNBR margin is comparable to that calculated for the reference Westinghouse 17x17 solid fuel (MDNBR of 1.58) at 100% power using the same assumptions and boundary conditions. The MDNBRs in the inner and outer channels of the 12x12 array are better balanced (inner/outer=1.42/1.51) than for the 13x13 design. The 13x13 design (inner/outer=1.37/1.93) has a slightly smaller MDNBR margin (by 4%) in the inner channels, but larger MDNBR margin (by 26%) in the outer channels than the 12x12 design. As will be seen in the next section, the MDNBR for the 13x13 assembly size can be increased, and better balance between the inner and outer annular channel can be achieved, through fine tuning of the cladding dimensions.

The pressure drop of the 12x12 design is 9% lower than the 13x13 design, as shown in Figure 6.22. Total pressure drops at 100% nominal power and flow rate are also shown and compared to the reference core pressure drop of 17x17 solid fuel. It can be observed that the annular fuel core has almost the same total pressure drop as the 17x17 solid fuel

at 100% nominal power and flow rate, although the annular fuel has much larger cooling surface. With a larger cooling surface and about the same flow area, the annular fuel has a smaller hydraulic diameter than the reference solid fuel core. The absence of grids in the inner channel and the larger hydraulic diameter of the outer channel balance out the effect of increased heat transfer surface. Hence, the annular fuel core has about the same total core pressure drop as the solid fuel core. The higher pressure drop of the annular fuel at 150% nominal power stems solely from the increased mass flow rate which is necessary to keep the core temperature rise the same.

Figure 6.21 MDNBR in the inner and outer channels for different assembly designs

The equilibrium qualities are plotted in Figure 6.23. It can be observed that at 18% overpower, the upper portion of both the outer and inner channels is above the saturation temperature and that the equilibrium quality of the outer channel is higher than that of the inner channel, even though the MDNBR in the outer channel is higher. This is due to the benefit of mixing in the outer channel. Furthermore, the quality of the hot outer channel is larger than that of the reference solid fuel design at 100% nominal power. This is primarily because the dimensions were selected such that more flow was forced intentionally into the inner channel, to balance the MDNBRs between the channels (MDNBR in the outer channel is higher due to mixing grids) and because of more pronounced flow depletion in the hot outer channel.

Figure 6.22 Total pressure drop of different assembly designs

DNBR results along the hot inner and outer channels of the best performance assembly size 13x13 at 150% power are compared to the DNBR along the hot channel of the 17x17 solid fuel at 100% nominal power in Figure 6.24. The DNBR values exceeding the value of 10 are not plotted and are kept at constant value of 10. It is interesting to note that the annular fuel exhibits significantly higher DNBRs than the solid fuel in the first two thirds of the core height and its minimum closer to the channel outlet. This is the result of the differences between the mass flux and qualities along the channels. In the lower half of the core, coolant quality is low, thus the differences of critical heat fluxes between the annular and solid fuel are determined more by the differences of their mass fluxes. The annular fuel core at 150% power (and 50% larger cooling surface) has 50% higher mass flux, so that DNBR is higher. While in the upper half of the core, the coolant quality becomes larger and the critical heat flux becomes determined more by coolant quality. Annular fuel core at 150% power has larger coolant quality in the upper half of the core (see Figure 6.23) so that the critical heat flux is somewhat lowered, thus DNBR decreases faster than that in the solid fuel case. The competition between the increased mass flux and coolant quality pushes the MDBNR locus for annular fuel closer to the exit.

Figure 6.23 Coolant equilibrium quality in the hot inner and outer channels of the 13x13 design at 150% power and in the hot channel of 17x17 solid fuel at 100% power

Figure 6.24 DNBRs in the hot inner and outer channel of the 13x13 design at 150% power and in the hot channel of 17x17 solid fuel at 100% nominal power

6.6.2 Rod Geometry Adjustment for MDNBR Balance

The whole core results discussed above showed that the 13x13 design has larger MDNBR in the outer channel than that in the inner channel. Therefore, further fine-tuning of the rod dimensions was performed to obtain better MDNBR balance between the inner and outer channels. In addition, adjustments to increase the fuel volume (the original 13x13 design had 89.6% of the reference solid fuel volume and the moderator to

fuel volume ratio was about 6% higher than that of the reference fuel assembly) were performed to match the moderator to fuel volume ratio to that of the reference core. A larger fuel volume is desirable for fuel management, as it helps prolong the cycle length.

In this analysis of the dimensions of the rods, the cladding and gap thicknesses were kept the same as in the previous design. The fuel volume was increased at the expense of a reduction in coolant volume, and the assembly moderator to fuel volume ratio was made equal to that of the reference solid fuel assembly. The resulting inner and outer diameters for various V_{fa}/V_{fs} ratios, where V_{fa} and V_{fs} are fuel volumes of the annular fuel assembly and the reference solid fuel assembly, respectively, are given in Table 6.8. The second column in the table shows the relative moderator to fuel ratio for annular and solid fuels and the last row lists the values for the original 13x13 design (PQN-02). The MDNBR values for each of the explored alternatives, as calculated using VIPRE-01 whole core model for 150% reference power and 150% reference flow rate, are summarized in Table 6.9.

Table 6.8 indicates that with the fuel volume increase at a fixed moderator to fuel volume ratio, both the inner and outer diameters of the annular fuel decrease while the thickness of the fuel meat increases.

Table 6.8 Geometries of alternative designs of 13x13 array size

V_{fa}/V_{fs}	$\frac{(V_c/V_f)_a}{(V_c/V_f)_s}$	Dco _o * (cm)	Dco _i (cm)	Df _o (cm)	Df _i (cm)	Dci _o (cm)	Dci _i (cm)
0.93	1.0	1.5592	1.4449	1.4325	1.0032	0.9908	0.8765
0.931	1.0	1.5542	1.4399	1.4275	0.9954	0.983	0.8687
0.932	1.0	1.5492	1.4349	1.4225	0.9877	0.9753	0.861
0.933	1.0	1.5442	1.4299	1.4175	0.9799	0.9675	0.8532
0.934	1.0	1.5392	1.4249	1.4125	0.9721	0.9597	0.8454
0.896**	1.06	1.5367	1.4224	1.41	0.99	0.9776	0.8633

* Subscripts, ci, f and co designate inner cladding, fuel and outer cladding respectively; second subscript designates outer (o) or inner (i) surface.

** Original 13x13 design

Table 6.9 MDNBR values for alternative 13x13 designs at 150% power and 150% flow

V_{fa}/V_{fs}		0.93	0.931	PQN-03 0.932	0.933	0.934	PQN-02. design
MDNBR	Outer	1.447	1.595	1.740	1.864	1.992	1.932
	inner	1.947	1.788	1.610	1.439	1.239	1.370
Pressure drop (MPa)		0.253	0.254	0.254	0.255	0.255	0.243

Table 6.9 shows that the MDNBRs in the inner and outer channels are better balanced for the annular/solid fuel volume ratio of 0.932 and the same moderator to fuel volume ratio. Compared to the original 13x13 design, the fine-tuned design has 4.8% higher core pressure drop and 3.6% more heavy metal in the core. The MDNBR of the

fine-tuned design (designated PQN-03) at 150% power is slightly higher than the MDNBR of the reference solid fuel at 100% power.

6.7 Sensitivity to Manufacturing and Operating Conditions

After the optimum annular fuel geometry has been identified, issues of sensitivity to the manufacturing tolerance and operating condition need to be addressed. These involve three main categories:

1. Sensitivity of MDNBR in inner channels and outer channels to manufacturing tolerances and oxide growth,
2. Sensitivity to gap conductance asymmetries. This issue is unique for the annular fuel, since a change in gap conductance on one side can significantly change heat flux to the other side and affect the MDNBR. This differs from the solid rod, where there is only one gap and changes in gap conductance affect only fuel temperature, but not MDNBR. Fuel performance analysis in Chapter 13 suggests that the annular fuel tends to expand during the in-core radiation with consequent increase of the outer gap conductance. The gap conductance asymmetry can become significant if the outer gap closes early in the irradiation while the inner gap remains open.
3. Sensitivity of DNBR results to changes of operating parameters, such as system pressure, core flow rate, core power and coolant inlet temperature. This larger sensitivity of MDNBR to these parameters is primarily because of: (i) the presence of isolated inner channels, where flow rate is determined by core-average pressure drop and friction pressure losses in the channel and cannot benefit from flow redistribution due to cross flow, and (ii) the smaller gap between two adjacent rods, i.e. the gap width for lateral flow in the annular fuel assembly (around 1mm) is smaller compared to that of the reference solid fuel assembly design (around 3 mm), reducing lateral communication between outer channels.

6.7.1 MDNBR Sensitivity to Manufacturing Tolerances and Oxide Growth

The MDNBR results in Tables 6.8 and 6.9 suggest a relatively large sensitivity to diameter changes. Comparing the cases of $V_{fa}/V_{fs} = 0.931$ and 0.932 , the inner diameter of the inner cladding decreases by 0.9% while the outer diameter of the outer cladding decreases by 0.32%. Thus, the flow area of the inner channel is decreased by 1.5% and the outer channel is increased by 1.5%. The VIPRE-01 results show that the flow rate in the hot inner channel is decreased by 4.2% and that in the hot outer channel is increased by 3.9%. On the other hand, the heat flux to the hot inner channel is increased by 0.7% and that to the hot outer channel is increased by 0.5%. However, considering the smaller surface area of the rod, the heat addition to the hot inner channel is decreased by only 0.2% and that to the hot outer channel is increased by 0.2%. The net effect is 4% higher and 3.6% lower enthalpy rise in the hot inner and outer channels, respectively. Higher enthalpy rise in combination with smaller mass flux in the hot channel results in a larger

MDNBR change in the hot channel. This sensitivity of MDNBR values to diameter change mainly arises from the flow distribution between the inner and outer channels as a result of increasing the outer channel flow area while decreasing the inner channel flow area, or vice versa. If the inner and outer diameters change in opposite direction so that the inner and outer channel flow areas change in the same direction, the sensitivity would be much smaller.

Nevertheless, the large sensitivity of MDNBR to dimensional changes raises a concern about the effect of manufacturing tolerances on the core thermal performance. The change of V_{fa}/V_{fs} from 0.93 to 0.933 results in the outer and inner diameters change by 0.015 cm and 0.023 cm in the same direction, but still satisfies the MDNBR requirement of 1.3. Achievable manufacturing tolerance is between ± 0.002 and ± 0.003 cm. This is much less than the above assumed diameter variation. VIPRE calculation for the tolerance of ± 0.002 to ± 0.003 cm show the MDNBR change by about $\pm 4\%$, which would be further mitigated by random distribution of plus and minus tolerances for the inner and outer diameters. It is also noted that the manufacturing process will have to achieve small surface roughness, which is about the same for both the inner and outer channels. VIPRE-01 calculations were performed using smooth surface friction factor correlations, since reactor core fuel rods, with smooth surfaces are typically achieved.

Another concern is the effect from oxide growth. Although it is small, the oxide growth will affect the flow area, thus flow rate, in the hot channels; further, the oxide layer has smaller conductivity and thus may have some effect on the heat split. To evaluate sensitivity of the MDNBR in the hot channels, VIPRE-01 whole core calculations were used for an oxide thickness of $\delta = 0.05$ mm which is a typical value at the end of in-core life (see Chapter 13). The chemical form of the oxide is ZrO_2 , which has a density of 5.9 g/cm^3 and conductivity of $2 \text{ W/m}\cdot^\circ\text{K}$ [Samsonov, 1973] Density of zirconium is 6.4 g/cm^3 . Since ZrO_2 has larger molecular mass and lower density, thickness of the cladding will be increased after oxidation. For simplicity and conservatism, this oxide growth layer was assumed to cover the whole surface of the hot rod along the entire height. Diameters of the rods with oxidation were calculated as follows:

For outer cladding:

$$D'_o = D_o - 2\delta, V_{Zr} = \frac{\pi}{4} [D_o^2 - D_o'^2], M_{Zr} = 6.4 \frac{\pi}{4} [D_o^2 - D_o'^2]$$

$$M_{ZrO_2} = \frac{123}{91} M_{Zr}, V_{ZrO_2} = M_{ZrO_2} / 5.9,$$

$$D_o'' = \sqrt{\frac{4}{\pi} V_{ZrO_2} + D_o'^2}$$

where D_o is the original outer diameter; D'_o is the diameter inside the oxide layer; V_{Zr} and M_{Zr} are the volume and mass of the oxidized Zr, respectively; V_{ZrO_2} and M_{ZrO_2} are the

volume and mass of the ZrO_2 that is generated after the oxidation, respectively; and D''_o is the diameter after the oxidation.

Similarly for the inner cladding:

$$D'_o = D_o + 2\delta, V_{Zr} = \frac{\pi}{4} [D_o'^2 - D_o^2], M_{Zr} = 6.4 \frac{\pi}{4} [D_o'^2 - D_o^2]$$

$$M_{ZrO_2} = \frac{123}{91} M_{Zr}, V_{ZrO_2} = M_{ZrO_2} / 5.9,$$

$$D''_o = \sqrt{D_o'^2 - \frac{4}{\pi} V_{ZrO_2}}$$

where the meaning of each variable is similar to that in the expression for the outer cladding with the inner cladding diameter, D_i , replacing D_o . The values of the diameter changes using these expressions are shown in Table 6.10.

Table 6.10 Diameter changes after oxidation

	δ	D_o	D'_o	D''_o	Δ
Outer (cm)	0.005	1.5367	1.5267	1.5413	0.002301
Inner (cm)	0.005	0.8633	0.8733	0.8586	-0.002332

Surface roughness is assumed unaltered after oxidation. The MDNBR results for the 13x13 fuel calculated by VIPRE-01 whole core model are shown in Table 6.11.

Table 6.11 MDNBR results calculated by VIPRE-01 whole core model

	Oxide on both surfaces	No oxidation
Inner channel	1.415	1.370
Outer channel	1.861	1.932

There are two effects of oxide growth on one side of the annular fuel rod: (i) a decrease of the flow area; (ii) an increase of the thermal resistance on this side. The first effect will reduce the MDNBR on the affected side since the mass flow will be smaller and the second will increase the MDNBR on this side due to the reduced heat flux. The results from Table 6.11 indicate that the MDNBR of the outer channel is reduced slightly and that of the inner channel is increased when oxide growth exists on both sides. While the assumptions for this analysis are very conservative, the results show that effects of oxide growth on MDNBR are small.

In conclusion, the sensitivity results show that MDNBR changes due to manufacture tolerance and oxide growth on the surfaces of the cladding are relatively small and the MDNBR limit can be satisfied at 150% power.

6.7.2 Gap/contact Conductance Sensitivity Analysis

Calculations performed so far assumed that the gap conductances of the outer and inner gaps are identical. This may not hold during the entire irradiation history of a fuel rod because fuel will expand, crack, swell and relocate. It was found in that work that the outer gap conductance tends to increase after thermal expansion of the annular fuel ring which leads to contact with the outer cladding. The actual behavior of both gap conductances depends on the irradiation history. Following the fuel performance analyses results, we assume in this sensitivity study a reduction of the inner gap conductance by 10% and an increase of the outer gap conductance by 30% from their reference values. As discussed in the introduction to this section, gap conductances of annular fuel affect not only fuel temperature, but also MDNBR because of the changes in heat fluxes on each side. Therefore, MDNBR sensitivity to these effects was evaluated using the VIPRE-01 whole core model for both the pellet fuel (gap conductances) and VIPAC fuel (contact conductances). Analysis was performed for the 13x13 array (PQN-02 design) at 150% nominal power with 18% overpower and 150% coolant flow rate.

DNBR results for the inner and outer channels, when the inner gap conductance, h_{gi} is decreased by 10% and outer gap conductance, h_{go} , increased by 30% are plotted in Figure 6.25. The DNBR results in the inner and outer channels with the reference gap conductance are also shown for comparison.

Figure 6.25 DNBR sensitivity to gap conductances

Clearly, DNBR is quite sensitive to asymmetries in gap conductances. For the case of 10% smaller h_{gi} and 30% larger h_{go} the MDNBR in the outer channel is reduced from about 1.93 to 1.46, and that in the inner channel is increased from 1.37 to 2.11. This is because the gaps represent significant part of the thermal resistances for heat removal from the fuel to coolant, and thus once the gap conductance is increased on the outside,

more heat is driven toward the outer channel. Benefiting from the large margin of MDNBR in the outer channel at the reference condition, the fuel meets the DNBR requirement at this gap conductance asymmetry. The sensitivity of MDNBR to gap conductance changes poses a new challenge not observed in solid fuel rods. It is noted, however, that the analysis above assumes the same gap conductance along the entire rod. In reality, random distribution of the gaps will reduce the sensitivity of MDNBR to gap conductance asymmetries.

The situation does not materialize in the VIPAC rods, which do not have gaps between fuel and cladding and the contact conductance is much larger, (e.g., 25,000 W/m²-K [Cunningham et. al., 1981] as opposed to 6,000W/m²-K for pellet fuel with open gap). In addition, fuel movements are restricted in the absence of gaps and sintering at such low temperatures is not expected to occur. Thus, only changes from irradiation driven fuel relocations may occur early during burnup and will most likely be small. As a result, much smaller asymmetries between gap conductances than in pellet fuels can be expected. Nevertheless, the same changes of contact resistances were modeled for VIPAC fuel.

Since VIPRE-01 does not provide the option of adding thermal contact resistance at the interface between materials, this contact resistance has to be included into the cladding by adjusting its conductivity so that the same total (cladding plus contact resistance) thermal resistance is achieved. This simplification will not affect heat flux split, DNBR, fuel temperature or cladding outside temperature; only cladding average temperature will be increased. The new effective conductivity of the cladding can be calculated from the equality:

$$\frac{1}{2\pi k'_c} \ln\left(\frac{R_{co}}{R_{ci}}\right) = \frac{1}{2\pi R_g h_g} + \frac{1}{2\pi k_c} \ln\left(\frac{R_{co}}{R_{ci}}\right) \quad (6-6)$$

where k'_c is the new effective conductivity of clad including the contact conductance, k_c is the actual conductivity of Zircaloy cladding, R_{co} and R_{ci} are the inner and outer radii of the cladding, h_g is contact conductance based on R_g ; here R_g is equal to R_{ci} and R_{co} for outer and inner cladding, respectively. Recasting Eq. (6-6) yields

$$k'_c = \frac{R_g h_g k_c \ln\left(\frac{R_{co}}{R_{ci}}\right)}{k_c + R_g h_g \ln\left(\frac{R_{co}}{R_{ci}}\right)} \quad (6-7)$$

Figure 6.26 plots the DNBR comparison in the inner and outer channels. For the same fractional changes in contact conductance as for the pellet fuel case, DNBR for VIPAC rods is significantly less sensitive. For example, MDNBR in the outer channel is reduced only from 1.99 to 1.84, and that in the inner channel is increased from 1.37 to 1.52. This is because now the major heat transfer resistance from fuel to coolant is primarily in the fuel meat, while the fuel/clad thermal resistance is very small due to the large contact

conductance. Therefore, VIPAC fuel is much more resilient to potential asymmetries in fuel/clad contact conductance than the pellet fuel.

Figure 6.27 DNBR sensitivity to gap conductances for VIPAC fuel

6.7.3 MDNBR Sensitivity to Changes in Operating Parameters

In this sensitivity analysis, the base case for annular fuel (the 13x13 PQN-02 design) is at 150% power with 2% overpower, 150% core flow rate, 292.7 °K inlet coolant temperature, and 15.5 MPa system pressure (at outlet). The base case for solid fuel is at 100% power with 2% overpower, 100% core flow rate, and the same core inlet temperature and system pressure as for the annular fuel. MDNBR results were calculated by VIPRE-01 whole core model in such a manner that one parameter was changed while the other core parameters remained fixed. The calculated results of MDNBR sensitivity to changes in core pressure, core power, coolant inlet temperature, and core flow rate for both designs are shown in Figures 6.29 through 6.32.

The figures show that the annular fuel core has somewhat larger MDNBR sensitivity to all core operating parameters considered. The results confirm the expectation that the MDNBR in the inner channel, which is isolated and thus cannot benefit from flow mixing, will be more sensitive to changes in operating parameters. However, it can be also observed that the outer channel MDNBR is more sensitive to operating parameters than the reference solid fuel. This was a surprising finding, hence an artificial model with only “outer” channels (i.e. outer-channel-only model, OCOM) was set up to identify the reasons for this behavior. The rods in this model were defined as fuel rods with the same outer diameter as the optimized 13x13 annular fuel rods. The core power and core flow rate for the base case were approximately equal to the heat rate removed through the outer channels and the flow rate in the outer channels in the regular annular fuel core. This model eliminated the effect of the inner channel and investigated solely the outer

channels. The calculated results of MDNBR sensitivity to changes of core pressure, core power, coolant inlet temperature, and core flow rate for both designs are also shown in Figures 6.29 through 6.32 (curves “outer channels only model (OCOM)”).

Figure 6.29 MDNBR sensitivity to core pressure

Figure 6.30 MDNBR sensitivity to core power

Figure 6.31 MDNBR sensitivity to coolant inlet temperature

Figure 6.32 MDNBR sensitivity on core flow rate

It can be observed that the sensitivity of the new outer-channels-only core is almost the same as that of the annular fuel core with both internal and external channels, and is larger than that of the reference solid fuel core. The MDNBR sensitivity of the outer-channels-only core is almost the same as for the inner channels in spite of the cross flow, which is possible among outer channels. The main difference between an outer-channels-only and the reference solid fuel is that the outer-channels-only assemblies based on the annular fuel assembly have smaller rod-to rod gap width between two adjacent rods (around 1mm) than that of the reference solid fuel assembly (around 3mm).

To quantify the gap width effect, a third case of larger gap width (the same value as for the reference solid fuel assembly design) was set up and calculated using the reduced outer diameter of the fuel rods of the outer-channel-only model (OCOM). The results of MDNBR sensitivity to changes of operating parameters for both designs show that the MDNBR sensitivity of the outer-channels-only model with larger gap width is the same as that of the reference solid fuel core (see Figures 6.29 to 6.32). Since a small gap width will limit lateral flow and flow mixing between the channels, it can be inferred that this larger sensitivity arises from the isolation effect between the channels. One can infer from the above studies that a core consisting of non-communicating (or with large resistance to cross flow) parallel multiple channels will have larger DNBR sensitivity to core parameter changes. However, it is noted that all the above calculations were performed with zero mixing coefficient. The results of OCOM with $\beta = 0.08$ (refer to Equation 6-3), also plotted in Figures 6.29 through 6.32, show that the sensitivities are now smaller. This once again confirms that the large sensitivities are the consequence of limited flow communication between the channels.

6.8. Power Uprate Considering Limitation on Reactor Coolant Pump Size

It has been shown that the use of internally and externally cooled annular fuel has the potential for increasing the PWR core power density up to 150%, if coolant flow rate is also proportionally increased to 150%, thus keeping the coolant outlet temperature unchanged. Larger core flow rate can be achieved by installing more powerful reactor coolant pumps or adding more primary loops. A recent study by Westinghouse [2004] pointed out that it may be difficult to achieve a coolant flow rate increase by 50% because of limitations of the capacity of current pumps, if no new loops are added. Fifty percent flow rate increase requires reactor coolant pumps to provide about 2.25 (1.5^2) larger head and the pump motors to have approximately 3.375 (1.5^3) larger pumping power. Westinghouse provided two alternative scenarios to avert such pumping power increase, with MATHCAD files to calculate the new core parameters based on typical configuration of Westinghouse 4-loop plants [Westinghouse, 2004]. The two cases are:

Case 1, where the reactor coolant pumps (RCPs) are not replaced; and

Case 2, where the RCPs are replaced with larger pumps, but the maximum pump power is limited to twice that of current RCPs.

The purpose of this investigation is to evaluate the required core inlet and outlet temperatures for flow rates consistent with the pump power restrictions, such that the

MDNBR safety limit is not exceeded. The DNBR calculations were performed using VIPRE-01 annular fuel whole core model. The MDNBR safety limit was taken to be equal to that for a regular Westinghouse 4-loop PWR with solid fuel, and using the same analysis approach.

The safety analysis limit for the MDNBR has been defined. To be a conservative estimate, for Critical Heat Flux (CHF) predicted by W-3 correlation, the minimum DNBR has to be larger than 1.3 at all times. For this steady-state analysis, conservative conditions (18% overpower and 2 °C increased coolant inlet temperature) were applied to calculate MDNBR in an effort to address the spectrum of Condition I and Condition II events. The safety analysis MDNBR limit adopted for judging the acceptability of the new fuel was taken to be the same MDNBR as obtained for the reference Westinghouse 4-loop design using the same analysis method and assumptions. Further, to be consistent on flow rate, the core flow rate for the reference solid fuel core was also calculated by the MATHCAD files provided by Westinghouse using the core friction coefficient calculated by VIPRE-01. In the VIPRE-01 whole core model of the reference solid fuel core, the hot pin was assumed to have a power peaking of 1.65 and other detailed specifications were similar to those of the annular fuel whole core model. The calculated MDNBR of the reference solid fuel core was 1.576, and this value was used as the DNBR limit for this power uprate analysis. Both calculations use effective core flow rate, i.e., minus core bypass.

Figure 6.33 shows the MDNBR results for various outlet coolant temperatures for Case 1 with RCPs not changed and core power uprate to 125% of current core power. $\dot{M}_{CORE}$, the core mass flow rate, and T_{in} , coolant inlet temperature, were calculated by the MATHCAD files provided by Westinghouse given the coolant outlet temperature and core power. The MDNBR results and core pressure drop were calculated by VIPRE-01 whole core model. From Figure 6.34, the coolant inlet temperature limits for this case are around 277 °C and 279 °C for PQN-02 and PQN-03 respectively. At the limit, the coolant inlet temperatures are around 15 °C and 13 °C lower than the reference core, and the hot leg temperatures (T_{HOT}) are around 8 °C and 6 °C degrees lower than the reference core for PQN-02 and PQN-03 respectively.

Similarly, Figure 6.34 shows the results of DNBR values for Case 1 with RCPs not changed, but for the core power uprate to 150%. Only PQN-03 can meet the DNBR limit. At the limit, the coolant inlet temperature would have to be lowered to around 261.5 °C, which is around 30 °C lower than the reference case. Such a large temperature reduction does not appear to be promising.

For Case 2, the core power was uprated to 150% of current core power and the RCPs were replaced, but their power was limited to twice that of current RCP power based on current technology [Westinghouse, 2004]. The DNBR results (at various outlet coolant temperatures) are shown in Figure 6.35. From Figure 6.35, the coolant inlet temperature limits for this case are about 279 °C and 281 °C for PQN-02 and PQN-03 respectively. At the limit, the coolant inlet temperatures are about 13 °C and 11 °C lower than the reference core and the hot leg temperatures (T_{HOT}) are around 8 °C and 6 °C degrees lower than the reference core for PQN-02 and PQN-03 respectively.

Figure 6.33 MDNBR results for power uprate to 125% without RCPs replacement

Figure 6.34 MDNBR results for power uprate to 150% without RCPs replacement

Figure 6.35 MDNBR results for power uprate to 150% with RCPs replaced and their power doubled

In conclusion, 25% and 50% power uprates are feasible for the same RCPs and for RCPs of double power, respectively. In both cases, core inlet and outlet temperatures are reduced leading to lower plant efficiency, which is further reduced in case of larger pumps due to the higher RCP power consumption. An optimization process will be necessary to find the best new RCP power and operating parameters for this scenario. This has been performed under a separate NEPO project at Westinghouse and EPRI. Our preliminary calculations suggest that the 150% thermal core power with larger RCPs and 10°C lower core inlet temperature will result in about 1% loss of plant efficiency. Thus, a net uprate of about 45.6% will be feasible if we assume that the reference power plant has a total thermal efficiency of 34%.

6.9 Flow Instability Analysis

Parallel non-communicating channels are more susceptible to instability phenomena than communicating channels, thus the resistance of the annular fuel to flow instabilities needs to be analyzed. Two major instability types – excursive (Ledinegg) instability, and density wave oscillations – were evaluated for the 13x13 annular fuel design.

6.9.1 Ledinegg Instability

Ledinegg instability is important in low-pressure systems; high-pressure systems such as the PWR are much less susceptible to this phenomenon. The susceptibility to Ledinegg instabilities was evaluated using the whole core VIPRE-01 model. The core

with the 13x13 PQN-02 assembly design was assumed to operate at high power (150% reference power) and the operating characteristics of the hot inner and outer channels were calculated for the range of the flow rates between 60% and 140% and system pressure of 15.5 MPa. The results in Figure 6.36 show that, even at low core flow rate of 60% and high exit equilibrium quality of 0.25 in the hot inner channel (see Figure 6.37),

Figure 6.36 Whole core pressure drops at various core flow rate at nominal power and pressure

Figure 6.37 Change in exit equilibrium quality in the hot inner and outer channels with core flow rate

Figure 6.38 MDNBR in inner and outer channels as a function of core flow rate

no reverse trend in pressure drop (bottom of the S-shaped curve) occurs. It can also be observed from Figure 6.38 that the MDNBR=1 condition is encountered in the inner channel for A flow rate of 77% before a Ledinegg-type instability, hence the DNBR criterion is more limiting than the flow excursion instability limit.

The whole core model results confirm the expectation that Ledinegg instability is not a problem for the PWR cores employing annular fuel.

6.9.2 Density Wave Oscillations

Density wave oscillations typically appear in systems consisting of combined sections of single-phase and two-phase flow, such as the hot channels of a core. Density wave oscillations depend on many factors. High inlet velocities, high system pressure, large inlet pressure losses and large inlet subcooling increase channel resistance to density wave oscillations. Conversely, large channel length, high heat flux and large bypass flow in parallel channels tend to destabilize the channel [Borkowski et. al., 1993]. The coolant channels of annular fuel are relatively long and operate at high heat flux, which favors instability. On the other hand, they possess stabilizing factors since they operate at high inlet velocity, large inlet subcooling and high pressure. Also, the two-phase flow section in the hot outer channel is very small (about 80cm compared to a 282 cm-long single-phase section). The inner channel remains subcooled along the whole active length. Hence it is not expected that the inner and outer channels will be susceptible to density wave oscillations. This expectation is confirmed by the stability map on Figure 6.39 generated using the simplified stability criterion of Saha et al. [Saha et. al., 1976]. Saha recommends construction of a stability map in an equilibrium phase change number-subcooling number space where the equilibrium phase change number is defined as

$$N_{pch,eq} = \frac{\rho_f - \rho_g}{\rho_g} \cdot \frac{q'' P_h L}{m h_{fg}}, \quad (6-8)$$

and the subcooling number is defined by the relation

$$N_{sub} = \frac{\rho_f - \rho_g}{\rho_g} \cdot \frac{h_f - h_{in}}{h_{fg}}, \quad (6-9)$$

In the above equations, ρ stands for density, P_h for heated perimeter, m for mass flow rate, L for heated length, h for enthalpy and h_{fg} for latent heat. The subscripts g and f denote saturated state in vapor and liquid phase, respectively. The map consists of two lines. The first line is a line of constant phase change number obtained at zero subcooling, $N_{pch,eq,0}$. This number has been obtained using the simplified stability criterion of Ishii [Ishii, 1971]

$$\frac{\rho_f - \rho_g}{\rho_g} X_{e,eq} = \frac{2 \left(K_{in} + \frac{f_{TP} L}{2D_e} + K_e \right)}{1 + \frac{1}{2} \left(\frac{f_{TP} L}{2D_e} + 2K_e \right)}, \quad (6-10)$$

as

$$N_{pch,eq,0} = \frac{\rho_f - \rho_g}{\rho_g} X_{e,eq}, \quad (6-11)$$

where K_{in} and K_e are form losses at the channel inlet and exit, respectively, $X_{e,eq}$ is equilibrium channel exit quality and f_{TP} is a two-phase friction factor. The two-phase friction factor was calculated using the Homogeneous Equilibrium Model (HEM). The HEM method is conservative for estimating the onset of density wave oscillation. [Zhao et al., 2005] To obtain exit equilibrium quality at the onset of density wave oscillations, coolant conditions at the channel inlet were set to saturation and the power into the channel (keeping the axial heat flux profile fixed) was increased until the equality satisfying Eq. (6-10) is reached. Note that iterations are necessary since both the RHS and LHS of Eq. (6-10) vary with changes in channel power. Moreover, redistribution of mass flux in the parallel channels due to power changes must be also considered. The outer channel was found to be the first to reach the density wave instability for zero subcooling at 185% power above the 150% uprated core (peak linear heat rate of 210kW/m in comparison with 110kW/m at 150% power density) with exit quality $X_{e,eq} = 0.72$, which yielded $N_{pch,eq,0} = 3.5$ by virtue of equation (6-11). This number establishes point A on the stability map, as shown in Figure 6.40.

The second line is a constant subcooling number line, established by intersection of the equilibrium phase change number and the subcooling critical number calculated from nonequilibrium theory [Saha et. al., 1976], i.e.,

$$N_{sub,cr} = 0.0022Pe \frac{A}{P_h L} N_{pch,eq,0} \quad \text{for } Pe < 7 \times 10^4, \text{ and} \quad (6-12)$$

$$N_{sub,cr} = 154 \frac{A}{P_h L} N_{pch,eq,0} \quad \text{for } Pe \geq 7 \times 10^4. \quad (6-13)$$

where Pe is the Peclet number. This intersection is shown as point B in Figure 6.40. The last point, C, is calculated as the intersection of the critical subcooling number line and Ishii's stability criterion line, i.e.,

$$N_{pch,eq,c} = N_{sub,cr} + \frac{\rho_f - \rho_g}{\rho_g} \left(\frac{\bar{q}_{cr}'' P_h L}{m h_{fg}} - \frac{h_f - h_g}{h_g} \right) \quad (6-15)$$

where $\bar{q}_{cr}''$ is the average channel heat flux at 185% power, i.e., the power which initiates density wave instabilities at zero subcooling. The stability map confirms that the operating point lies deeply in the stable region.

Figure 6.39 Simplified stability criterion for thermally induced flow oscillations in the outer coolant channel

Because the above calculations are based on an isolated channel approach and overestimate the flow rate to the hot inner channel, a more rigorous approach using a whole core VIPRE-01 model, which incorporates cross flow effects on the pressure drop

determining flow rate into inner channels, was also used. In addition, VIPRE incorporates a variety of void fraction and pressure drop models covering the high void fraction region, where the HEM model used in the isolated channel model may not be appropriate.

Figure 6.40 plots outlet channel qualities (X_{exit}) and equilibrium-limit exit channel qualities, defined as Eq. (10) as ($X_{exit,eq}$), for the inner and outer subchannels of the hot channel for zero inlet subcooling and various linear heat rates. It can be observed that as the linear heat rate increases, the outlet quality in the outer subchannel reaches the equilibrium-limit quality faster than the inner channel. Because mass fluxes through the hot inner and outer channels in the whole core model are smaller than those in the isolated channel model, the slope of the exit quality lines is steeper and f_{TP} is smaller, thus $X_{exit,eq}$ is smaller than that from the isolated model. As a result, the outer cooling channel is first to reach the density wave instability for zero subcooling at 139% power above the 150% uprated core (linear heat rate of 164 kW/m in comparison with operating value of 118 kW/m at 150% power density) with exit quality $X_{e,eq}=0.62$, which yields $N_{pch,eq,0}=3.0$ (see Figure 6.41) to be included as point A on the stability map in Figure 6.42.

Figure 6.40 Comparison of outlet quality and equilibrium-limit exit quality for zero inlet subcooling for the inner and outer channels

Figure 6.41 Constant phase change number at zero subcooling, $N_{pch,eq,0}$

Figure 6.42 Simplified stability criterion for thermally induced flow oscillations in the outer coolant channel

Finally, point C was established from the subcooling critical number per Eqs. (8-5,6) and Ishii's stability criterion line after Eq. (8-7). The resulting stability map, including the operating point, is shown in Figure 6.42. As in the Ledinegg stability analyses, the whole core model results confirm that the operating point at nominal operation for 150% power density is deeply in the stable region. Therefore, flow instability is not an issue for the proposed annular fuel, in spite of the fact that the inner channels are isolated and do not communicate with adjacent channels. A high system pressure and a small channel fraction experiencing boiling, in the form of subcooled boiling with small void fraction, are the main factors for operation far from instability line. As observed from the analysis, MDNBR is more limiting to power increase than instabilities.

6.10 Annular Fuel Application to PWR with Hexagonal Lattices

Currently, a significant number of PWRs with hexagonal lattice, designated VVERs, operate in Russia and Eastern and Central Europe. Western fuel manufacturers offer and supply fuel for these reactors. VVER is the Russian edition of PWR with a triangular lattice assembly design. Recently, Westinghouse performed modernization of I&C and core design of VVER 1000 at Temelin, Czech Republic, and supplied fuel designated VVANTAGE 6. Temelin core data with VVANTAGE 6 [Gavrilas et. al., 2000] fuel were used as a reference design for this evaluation. VVER 1000 core characteristics are compared with those of typical Westinghouse 4-loop PWR in Table 6.12. The whole core VIPRE-01 model of the VVER 1000 core with both the solid fuel and annular fuel was developed using similar approaches as described earlier to identify the best geometry and potential uprate of VVER-1000. The details of the model are given in [Kazimi et al., 2003].

Table 6.12 VVER 1000 versus Westinghouse 4-loop PWR core data

Parameters	VVER 1000 VVANTAGE6	WESTINGHOUSE 4- LOOP
Thermal output	3000MW	3411MW
Number of fuel assemblies	163	193
Number of fuel rods/fuel assembly	312	264
Fuel active height	3630mm	3660mm
Diameter of guide thimble tube	12.6mm	12.243mm
Diameter of fuel rod	9.144mm	9.5mm
Fuel rod pitch	12.75mm	12.6mm
Fuel assembly pitch	236mm	215mm
Nominal pressure	15.7MPa	15.51MPa
Inlet temperature	288.11°C	292.7°C
Effective mass flux	16043kg/s	17700Kg/s
Average mass flux	3878.8kg/m ² /s	3729kg/m ² /s

The optimum dimensions of annular fuel to fit in the hexagonal assembly of fixed size were searched in such a manner to obtain the best thermal hydraulic performance and keep the same fuel to moderator volume ratio as for the solid fuel. Because the annular fuel rods have to be larger to provide for sufficient flow in the inner channel, fewer rods can be placed within fuel assembly of a given size. The number of rods (including fuel

rods and control rods, or thimble tubes) in a hexagonal assembly can be obtained from the relation $3n^2 - 3n + 1$, where n is the number of rods on each side of the hexagon. The VVER original assembly design has 11 rods on each side, with a total number of rods of 331 (312 fuel rods and 19 thimble tubes). Two most promising arrays of annular fuel rods were investigated – 9 rods on each side of the hexagon (designated as 9^2 , or PXN-02), which has 217 rod positions (204 fuel rods and 13 guide tubes) and 8^2 (PXN-01) arrangement having 169 rod positions (156 fuel rods and 13 guide tubes). Figure 6.43 compares the layout of the reference VVER FA (11^2) with 9^2 and 8^2 layouts.

Figure 6.43 Hexagonal FA layouts

The search for the optimum dimensions was performed under similar constraints as for square lattice. Table 6.13 lists the best performing designs of 8^2 and 9^2 assembly sizes with the ratios of annular fuel to solid fuel volumes while keeping moderator to coolant volume ratio the same as for the reference solid VVER fuel. The first c and f subscripts stand for cladding and fuel, respectively, the second subscript o and i stand for the outer and inner claddings, and the third subscript o and i denotes the outer and inner diameters. The MDNBRs in the inner and outer channel at 150% power calculated by W3-S and W3-L correlation are also shown in the table. These MDNBR values can be compared to MDNBR=1.46 obtained for the reference VVER-1000 core with solid fuel using the W3-L correlation and consistent VIPRE-01 modeling assumptions.

Table 6.13 Dimensions of annular fuel in a hexagonal 8^2 array and 9^2 array and MDNBR values at 150% power uprate

Annular/solid fuel volume ratio	Pitch (cm)	D _{COO} (cm)	D _{COI} (cm)	D _{FO} (cm)	D _{FI} (cm)	D _{CIO} (cm)	D _{CII} (cm)	MDNBR
								Inner/Outer
8^2 design								
0.950	1.785	1.5681	1.4538	1.4381	0.9482	0.9325	0.8182	1.61/1.96
9^2 design								
0.925	1.558	1.3844	1.2701	1.2544	0.8385	0.8228	0.7085	1.88/1.92

It can be observed that the same power uprate can be obtained also for VVER-1000 cores with hexagonal lattice. The 9×2 design achieves better MDNBR balance, than the best 8×2 design. However, the 8×2 design was selected as the most promising for the following reasons: (1) it requires a smaller number of annular fuel rods to be manufactured, (2) has lower core pressure drop by 14.1%, (3) it allows loading of more fuel, and (4) it provides larger MDNBR margin in the outer channel, where more heat flux is expected due to preferential outward fuel expansion after heatup and during burnup. It is also interesting to note that the number of rod positions (169) of the best performing design is the same for both the square and hexagonal assembly. Therefore, the overall conclusion is that the application of annular fuel to VVER 1000 core would yield about the same benefit of power uprate by 50% as for the PWR cores with square lattice.

It should be noted that the hexagonal lattice is better for the application of annular fuel in a sense that it makes it easier to force more flow into the inner channel to balance the DNBR between the inner and outer channels. The smallest flow area of the outer channel of square lattice is limited by the 1 mm minimum value of rod-to-rod gap width which still leaves relatively large openings at the corners of the square cell. For example, in the 14×14 square lattice size, because of the minimum gap limitation and the restrictions on fuel volume and coolant volumes, the DNBR in the inner and outer channels could not be balanced without increase of grids pressure drop. Because, the additional pressure drop is undesirable, 14×14 array was rejected. In the case of hexagonal lattice, the triangular arrangement of the rods allows one to achieve a balanced MDNBR for larger array sizes (smaller rods) with low pressure drop grids. The rod-to-rod gap widths of 8×2 and 9×2 arrays in Table 6.13 are 2.17 mm and 1.74 mm respectively, i.e., larger than that of the best 13×13 square lattice design (1.14 mm). Larger gaps allow better cross flow communications thus reducing the outer channel MDNBR sensitivity to operating parameters and are easier to manufacture.

6.11 Summary and Conclusion

Thermal hydraulic analysis of the high power density internally and externally cooled annular fuel was carried out using various tools to identify the most promising configuration and potential for power uprate. A VIPRE-01 methodology for the analysis of the internally and externally cooled annular fuel using heat generating tubes option of VIPRE-01 was developed. This served as a basis for the MDNBR analysis of whole PWR cores with annular fuel.

The whole core VIPRE-01 model was used to identify the optimum dimensions of annular fuel rods and assembly array size. The most promising options, based on MDNBR considerations, were found to be the 13×13 and 12×12 arrays. Because fuel performance modeling results showed that thermal expansion and swelling of fuel pellets during operation are towards the outer cladding and increase the heat flux to the outer channel, the 13×13 array that accommodates higher heat flux to the outer channel, was selected as the most promising design. The 13×13 design allows a power uprate of 50% in terms of DNBR limit, which allows 150% power to be extracted from the same core volume as the reference solid fuel at 100% power. This power uprate requires a

proportional increase of core flow rate to maintain the core temperature rise the same. Separate analyses have shown that such a large flow rate would be acceptable from the point of view of vibration due to high rigidity of large annular rods. However, the power requirements for the reactor circulation pumps may be too high and would require significant development of new pumps. Therefore, the possibility of using the largest available pumps with double the power of current pumps but smaller flow rate than 150% was also explored. The 50% power uprate was found possible with this smaller pump option and reduced core inlet temperature by 10°C. Lower core temperature reduces plant efficiency by 1% giving a net power uprate of 45.6%. Moreover, this large power uprate is possible at fuel peak temperatures substantially below the reference solid fuel, reducing fission gas release and improving LOCA performance. Similar power uprates were found to be possible for hexagonal lattice of VVER designs. The triangular arrangement of the rods is more capable of balancing the DNBR in the inner and outer channels.

Five issues involving manufacturing tolerances, oxide growth on the surfaces, inner and outer gap conductances asymmetry, more pronounced MDNBR sensitivity to changes of core parameter, and resistance to instabilities were also evaluated. The effect of industry manufacturing tolerances and of conservatively large oxide growth on MDNBR was found to be small and did not result in MDNBR values below the limit. The large sensitivity of MDNBR to gap conductances in the inner and outer gap was identified as the most important issue that requires further experimental investigation through irradiation programs. VIPAC fuel, which has high gap conductance, is not sensitive to this problem and can serve as a possible backup design to pellet fuel. Other possible solutions for the pellet fuel are listed in Chapter 13. Annular fuel is more sensitive to operating parameters, such as core flow rate, core power, core inlet temperature and systems pressure. This somewhat larger sensitivity is compensated by the larger DNBR margin at rated conditions gained from the new geometry with larger heat transfer surface and higher mass flux. Finally, annular fuel is resistant to flow instabilities, such as Ledinegg instability and density wave oscillation due to high system pressure and one-phase flow along most of the hot channel length.

7. SAFETY ANALYSIS

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The key results of steady-state analyses described in Chapter 6 involved the identification of the optimum geometry of the externally and internally cooled annular fuel and of the magnitude of the uprate possible with this geometry. It was also shown that the externally and internally cooled annular fuel can operate at 150% of the power density of current typical Westinghouse 4-loop Pressurized Water Reactor (PWR) with core flow rate proportionally being increased with the core power to keep the core outlet coolant temperature the same. Some conservative approaches were used in the steady-state analysis in an effort to address the Condition II and Condition III accidents. The goal of this Chapter is to verify that the new annular fuel design performs satisfactorily (exhibits at least the same or better response than the reference solid pin fuel) in selected incidents and limiting fault events.

In this Chapter, the results of selected safety analyses of a typical 4-loop Westinghouse PWR with annular fueled cores, specifically the Loss of Flow Accident (LOFA), Main Steam Line Break (MSLB), Large Break Loss of Coolant Accident (LBLOCA) and Rod Ejection Accident (REA), are summarized.

7.1 Selection of Transients/Accidents for Safety Analysis

7.1.1 Basis of Event Selection

Even at the conceptual stage of the annular fuel design, it was envisaged that the annular fuel rods should result in a superior safety performance compared to the solid fuel rods. This is primarily because of the enhanced surface area per unit volume that the annular geometry provides. This was expected to provide significant margin gains in ‘Departure from Nucleate Boiling Ratio’ (DNBR), which is an important safety parameter. In addition, very low fuel temperatures of annular fuel result in small stored energy and better expected LOCA performance. It was also determined that at least a few accidents needed quantitative evaluation before the safety performance of the annular fuel design could be confirmed.

A qualitative evaluation was conducted in order to determine the possible impact of annular fuel rods on the FSAR accidents and transients for a typical Westinghouse 4-loop PWR. The study [Kazimi, et. al., 2002] assumed that the annular fuel rods could be operated at the same as well as higher overall core power levels as the current PWRs. All events described in a typical FSAR Chapter 15 were evaluated to identify the transients and/or accidents that would need a more detailed quantitative evaluation.

The events identified for detailed quantitative evaluation were: Loss-of-coolant Accidents (LOCAs), Main Steam Line Breaks (MSLBs) and Rod Ejection accidents. It was concluded from the qualitative evaluation that

1. For LOCAs, there needed to be an assurance that the inner channel could support sufficient steam flow and reflood thermal-hydraulics such that the acceptance criteria could be met,
2. For MSLBs, DNBR limits should be met, and a return to criticality should not occur, and
3. For rod ejection accidents, the energy deposition (calories/gm) in the fuel should be low enough to maintain fuel integrity.

Another event, namely, the complete loss of primary flow or loss-of-flow accident (LOFA) was added to the above list for quantitative assessment since the minimum DNBR (MDNBR) was found to be more sensitive to core flow rate and core inlet temperature for the annular fuel in comparison to the solid fuel (see Chapter 2).

7.1.2 Brief Description of Selected Transients/Accidents

7.1.2.1 Loss of Flow Accident (LOFA)

The complete loss of primary flow or loss-of-flow accident (LOFA) is a Condition III accident. All the reactor coolant pumps (RCPs) are assumed to trip due to under-voltage or under-frequency of the power supply buses. Loss of driving force of the RCPs flow in the primary loops slows down the pumps due to flow resistances.

The pump speed is determined by the flow resistances and the inertia of the moving coolant and the RCPs. A reactor trip signal is generated when the core coolant flow rate reaches 87% of normal value. Reactor trip is assumed to have 1 second delay after the trip signal is generated. The minimum departure from nucleate boiling ratio (MDNBR) in the core typically reaches the minimum value right before the time at which core power starts to decrease exponentially due to reactor shut down while the core flow rate is at 87% normal value. The turbine is assumed to trip at the same time as the reactor trip and the main steam valve to be closed with 2 second delay. The acceptance criteria for LOFA are minimum DNBR >1 and reactor coolant pressure limit.

7.1.2.2 Main Steam Line Break (MSLB)

This accident is initiated by a break in the main steam line. The largest break size would correspond to a double-ended break. However, flow restrictors normally exist in the steam line at the exit of the steam generators. Hence the largest break size is limited to the size of the flow restrictor orifice. The accident causes an increase in heat removal from the reactor coolant system (RCS) and results in a reduction of reactor coolant temperature and pressure. Due to the negative moderator temperature coefficient (MTC), this cooldown causes an increase in the core reactivity and an increase in reactor power.

If the plant is initially at power, the reactor is automatically tripped at high reactor power signal and the main steam and feedwater line isolation valves are automatically closed. Decay heat is removed as necessary through the unaffected steam generators by venting steam from the main steam safety valves (MSSVs) and the atmospheric steam dump

valves (ASDVs). The auxiliary feedwater system supplies makeup water to the unaffected steam generators (SGs). In this event, the main concern is whether there would be any fuel damage due to the power increase. Fuel damage is precluded as long as the reactor is tripped before DNB occurs. Hence occurrence of DNB is monitored in the analysis, but it can be less than 1.0. Acceptance criteria include no fuel failure and reactor coolant pressure limit to maintain RCS integrity. .

7.1.2.3 Large Break Loss of Coolant Accident (LBLOCA)

A large break loss of coolant accident (LBLOCA) is the result of a double-ended guillotine pipe rupture of the reactor primary coolant system. It is classified as a Condition IV event, a limiting fault and it forms the design basis for the emergency core cooling system (ECCS).

A 200% break is assumed in the cold leg of one of the primary loops. This causes rapid depressurization of the primary system and the reactor trips at a low primary pressure signal. The reactor trip also causes the turbine and the main feedwater trips. At a certain set pressure, the intact leg accumulator starts injection of the emergency cooling water into the downcomer of the reactor pressure vessel. The safety injection and the auxiliary feedwater injection also begin after some time delay. A locked rotor condition is conservatively assumed for the reactor coolant pumps at the initiation of the break. The main purpose of the calculation is to determine if the peak cladding temperature in the reactor core would remain below the present regulatory limit of 2200° F (1204° C).

7.1.2.4 Rod Ejection Accident (REA)

The rod ejection accident (REA) is one of the specified transients addressed as a part of the licensing process for PWRs. A mechanical failure of the control rod drive mechanism is assumed such that the RCS pressure would eject the control rod and drive shaft to the fully withdrawn position. Industry practice is to assume that the rod is ejected from the core, but does not breach the RCS pressure boundary. The ejection of the control rod represents a sudden insertion of positive reactivity, causing a surge in core power. The reactivity worth of the ejected rod determines the rate of initial power increase. The increase in power causes a high neutron flux reactor trip (or a flux rate increase trip). However, it takes a few seconds for the unaffected control rods to insert into the core. The initial power increase, which occurs over milliseconds, is mitigated (self-corrected) by the fuel Doppler coefficient. The final power decrease occurs a few seconds later, caused by the insertion of the unaffected control rods. During this time, significant energy can be deposited in the fuel rod.

The licensing analysis of this event generally seeks to initiate the accident from the worst possible initial condition, with the maximum possible ejected rod worth, and calculate the energy deposition in the fuel. In licensing analyses, the energy deposition is required to be less than 280 calories/gram at any axial location in the fuel rod. This is the main acceptance criterion for this event. Other acceptance criteria include avoiding centerline melt conditions, and limiting the number of rods experiencing post-DNB conditions to less than 10%.

7.2 Plant Models and Steady States

A typical 4-loop Westinghouse PWR with core power of 3411 MWt has been selected as the reference plant. Three plant models were developed to analyze the transients/accidents discussed in Section 7.1. These are:

1. Reference plant with solid fuel at 100% power
2. Reference plant loaded with annular fuel at 100% power
3. Modified plant loaded with annular fuel at 150% power.

The standard Westinghouse 17x17 fuel assemblies were assumed for the reference case with solid fuel at 100% power. For the annular fuels, 13x13 fuel assemblies having the same overall assembly dimension as the solid fuel assembly were used. Figure 7.1 shows the general arrangement of both the solid and annular fuel assemblies and Table 7.1 shows the important fuel rod dimensions.

Figure 7.1 General arrangement of solid fuel assembly (Left) and annular fuel assembly (Right)

In Table 7.1, for the 13x13 annular fuel design, in the first subscript, c and f stand for cladding and fuel, respectively; in the second subscript, i and o designate inner cladding and outer cladding or inner and outer diameters of the fuel ring, respectively; and in the third subscript, i and o denote the inner diameter and outer diameter of the claddings (i=inner, o=outer). The dimensions of the reference solid fuel pin are also shown.

Table 7.1 Dimensions (cm) of annular fuel 13x13 design and reference solid fuel

Array	D_{cii}	D_{cio}	D_{fi}	D_{fo}	D_{coi}	D_{coo}	Pitch
13x13	0.8633	0.9776	0.99	1.4100	1.4224	1.5367	1.651
17x17-ref.	Solid pin	-	-	0.8255	0.8379	0.9522	1.263

The RELAP5-3D code [2003] and its variations including MARS² [Lee, et al, 2002], was the primary vehicle for conducting the analyses presented in this chapter. The input model for the reference case with solid fuel was developed first. The model was then modified to incorporate the annular fuel.

7.2.1 Model for Solid Fuel

The RELAP5-3D model of the reference Westinghouse 4-loop plant with solid fuel was developed from an USNRC reference model for SBLOCA. The primary system nodalization is shown in Figure 7.2. The affected loop (for LOCA and MSLB) is modeled separately as shown on the left hand side of the figure, whereas the remaining three intact or unaffected loops are lumped together as shown on the right hand side of the figure.

Figure 7.2 Primary System Nodalization for the Reference 4-Loop PWR

The following modifications to the original USNRC system nodalization were introduced:

² The initial work was performed using MARS as well as RELAP5-3D/ATHENA, version 2.1.0. During the calculations, it was discovered that the reflood model in these versions had shortcomings. Hence, the new version of RELAP5-3D/ATHENA (version 2.2.411) with improved reflood model was obtained and used for all LBLOCA analyses. The LBLOCA results presented here are from RELAP5-3D/ATHENA, version 2.2.411. The results for LOFA were from MARS and the MSLB and REA results were obtained using RELAP5/MOD3v.3.2, available at FRAMATOME-ANP. Finally, for comparative studies against the reference core, the same version of the code was always used for consistent results. .

a) Split downcomer for ECC bypass and penetration:

- Two downcomer channels representing a broken-side and a lumped intact-side downcomer annulus.
- Cross flow junctions with a loss coefficient of 50.0 were applied for lateral flow between the downcomer channels based on previous assessments to simulate the ECC bypass and penetration during a LBLOCA.

b) Fuel and core:

- Two flow channels, one representing a core-average (192 fuel assemblies) channel and the other a hot (1 fuel assembly) channel, having cross flow junctions for lateral flow between the channels.
- A single hot rod is embedded in the hot channel together with the hot assembly rods,
- Core bypass channel is modified to represent the physical geometry.

Table 7.2 shows the important parameters used in the core and fuel modeling. It is assumed that the initial core power is at 102% of full power and the total peaking is 2.558 with axial power profile of 1.55 chopped cosine. The ANS73 standard [1973] value multiplied by 1.2 is used for the core decay heat model after reactor trip for conservatism.

Table 7.2 Important Parameters used for Core and Fuel Modeling

Parameters	Models
Initial Power	3479.22 MWth (102% Ref. Power)
Transient Power	
- Decay Heat	ANS73*1.2
- Kinetics and Trip Reactivity	Conservative
Core & Fuel Modeling	
- # of Axial Nodes	16
- Core Flow Channels	Average (192 FA) and Hot (1 FA)
- Total Peaking	2.558 ($q'_{peak} = 47.75 \text{ kW/m}$)
Axial Power Distribution	1.55 Chopped Cosine
Hot Assembly Peaking	1.587
Hot Pin Radial Peaking	1.65 (embedded in hot channel)
- Direct Heating	0.0%
- Gap Conductance	Constant

Using the above models, steady-state initial conditions were obtained through simulations as shown in Table 7.3. Good agreement is achieved in comparison with the desired values.

Table 7.3 Steady-State Initialization

Parameters	Desired*	Simulated
Core Power (MWth)	3479.22	3479.22
Pressurizer Pressure (bar)	155.1	155.1
Cold Leg Temperature (K)	564.85	566.61
Hot Leg Temperature (K)	599.25	598.72
Total Loop Flow (kg/s)	18630.0	18633.0
Effective Core Flow (kg/s)	17700.0	17779.1
Bypass Flow Fraction (%)	5.0	4.74
SG Secondary Pressure (bar)	58.0	61.3

*Desired Condition are based on typical 4-Loop PWR

7.2.3 Annular Fuel (100% and 150% power)

For the annular fuel with 100% power, only the reactor core model was modified such that each fuel assembly channel was divided into two flow channels representing the inner and outer flow paths of the annular fuel. Thus, the core is modeled by 4 cooling channels (inner and outer for the hot and the average channels). Figure 7.3 shows the nodalization in the reactor core. The outer channels employ junctions to allow for cross flow between the outer channels, while the inner channels are non-communicating. Flow areas of the annular fuel channels were weighted based on the geometric volume ratio.

Figure 7.3 Core Nodalization for the Annular Fuel Model

An annular fuel rod was modeled by radially subdividing it into 12 rings which represent the inner cladding (2 rings), the inner gap (1 ring), the fuel pellet (6 rings), the outer gap (1 ring) and the outer cladding (2 rings). Convective heat transfer boundary conditions were imposed on both the inner and outer cladding surfaces. However, since the code did

not have the capability to model dynamic gap conductance of two gaps, i.e., the inner and outer gaps in a fuel rod, the gap conductance of both the gaps were assumed equal and maintained constant in all analyses. It should be noted that the solid fuel model also incorporated the same constant gap conductance for consistent comparison between the two fuels.

With these models, the steady state conditions were initialized and they were in good agreement with the desired conditions as shown in Table 7.4. Figure 7.4 shows a comparison of the initial temperature distribution of the annular fuel with solid fuel (both for 100% power). It should be noted that the average fuel temperature of the annular fuel is substantially lower than that of the solid fuel. This, in turn, will result in a lower cladding temperature during the transients including the LBLOCA blowdown phase in which the cladding heats up by redistribution of the initial stored energy in the fuel.

Table 7.4 Steady-state Results with Annular Fuel at 100% Power

Parameters	Desired	Simulated
Core Power (MWth)	3479.2	3479.22
Pressurizer Pressure (bar)	155.1	155.1
Cold Leg Temperature (K)	564.85	566.67
Hot Leg Temperature (K)	599.25	598.40
Total Loop Flow (kg/s)	18630.0	18862.0
Effective Core Flow (kg/s)	17700.0	18030.9
Bypass Flow Fraction (%)	5.0	4.41
SG Secondary Pressure (bar)	58.0	61.3

Figure 7.4 Comparison of Steady-state Peak Fuel Temperature Distribution in Solid Fuel and Annular Fuel (both at 100% power)

For the annular fuel with 150% power, the following additional changes were made to the above annular fuel model (100% power):

- a) The core power was increased to 150% of the reference value without changing the fuel geometry or the number of fuel assemblies. The decay heat was also proportionally increased.
- b) Rated mass flow rate and speed of the reactor coolant pumps (RCP) were increased to 150% of reference value and the rated head of the pumps was increased to twice the reference value in an effort to achieve 150% of the reference core flow rate. However, the cold and hot leg diameters were kept the same.
- c) The volume of the steam generator was increased to 150% of reference value in order to transfer 150% power to the secondary loop. This was realized by increasing the heat transfer surface and the cross section of the steam generators by 50%, but keeping the same height as the reference case.
- d) The safety injection flow rate was increased in order to remove the larger decay heat load in case of an accident.

Table 7.5 shows the steady-state results as calculated by RELAP5-3D for the annular fuel core operating at 100% and 150% powers. The results show that the desired steady-state conditions were achieved with about the same coolant temperature rise for both the power levels.

Table 7.5 Calculated Steady-state Coolant Parameters for Annular Fuel Cores

Parameters	100% power	150% power
Core Power (MWt)	3479.2	5218.8
Cold Leg Temperature (K)	566.7	567.3
Hot leg Temperature (K)	598.4	598.0
Core Flow Rate (kg/s)	18862.0	29475.0
Core Flow Rate Ratio	1.0	1.56

7.3 Loss of Flow Accident (LOFA)

7.3.1 Systems (MARS) Calculations

The sequence of loss of flow accident is discussed in Section 7.1.2. Figure 7.5 shows the system pressure, reactor power and the core flow rate results during the transient calculated by MARS [Lee et al., 2002] – a Korean version of RELAP5. The same parameters of the solid fuel core at 100% power during the same transient are also shown for comparison. The annular fuel at 150% power model reached the 87% normal core flow rate value around 1 second later than the solid fuel at 100% power during the transient, causing 1 second delay of the reactor shut-down. Slower coastdown during the

transient of the annular fuel at 150% power is because of SG enlargement as the number of tubes was increased by 50% and thus the flow resistance was somewhat lowered and flow inertia term was increased.

Figure 7.5 Primary pressure, core flow rate and core power during LOFA

7.3.2 MDNBR (VIPRE) Calculations

The above RELAP5 results were used as the boundary conditions for transient Minimum DNBR calculations using VIPRE-01 [Cuta et al. 1985] whole core model. This coupled transient calculation provides more detailed thermal-hydraulic results in the core since the VIPRE-01 whole core model of annular fuel had many (49) sub-channels [Kazimi et al. 2002]. More details on VIPRE-01 modeling of annular core are given in Chapter 2. The MDNBR results calculated by the VIPRE-01 whole core model for solid fuel (100% power) and the annular fuel at 150% power are compared in Figure 7.6.

It should be noted that at time zero, i.e., at nominal conditions, the annular fuel design uprated to 150% power has significantly higher MDNBR than that of the reference solid fuel at 100% power. The steady state analyses in Chapter 2, which were performed at overpower conditions (18% overpower, 2 °C increased coolant inlet temperature), showed similar MDNBRs for the uprated annular fuel and the reference solid fuel. The larger MDNBR of annular fuel at nominal conditions is due to its larger sensitivity to core power and flow rate changes than solid fuel.

After time zero, all the RCPs trip and coolant velocity decreases. MDNBR results of the annular fuel decrease faster than that of the reference solid fuel design mainly due to its larger sensitivity to coolant flow rate change. MDNBR results reach the minimum value when the reactor power starts to decrease due to reactor shut-down at low core flow rate

(87% of normal value). The minimum values of MDNBR for both designs are well above 1.3 and the annular fuel even at 150% power has a slightly larger margin. After reaching the minimum value, MDNBRs start to increase and the MDNBR results of the annular fuel increase much faster than those of the solid fuel. This is because the annular fuel has much lower fuel temperature, thus much smaller stored energy release after shut-down.

Figure 7.6 Comparison of MDNBR results of Annular Fuel uprated to 150% power vs. the Reference Solid Fuel at 100% power during LOFA

7.4 Main Steam Line Break (MSLB)

Generally, in current reactors with solid fuel rods, the MSLB event is analyzed at the full power and hot, zero power initial condition. Depending on the design of the reactor protection system (i.e., the different types of reactor trips and the set points), either one of these cases can be limiting. This is still expected to be the case for reactors with annular fuel. In this study, the full power initial condition was used to examine the difference in performance between the fuel types, with initial power at 100% and 150%.

The MSLB analysis examined a double-ended break in the steam line (essentially limited to the size of the orifice, 0.13 m^2), as well as a smaller (0.09 m^2) break size. Essentially, the larger the break size, the faster the energy removal rate from the secondary, which leads to a faster cooldown rate of the primary. However, the larger break size will also get to the low steam line pressure setpoint faster, which leads to a faster reactor trip. Investigating two break sizes essentially provides a comparison of which effect is stronger in terms of DNB margin. As it turned out, the results for the different break sizes were not much different. Hence, only the larger break size is presented here.

7.4.1 System (RELAP5) Calculations

The system thermal-hydraulic analysis was conducted with the RELAP5/MOD3 computer code [USNRC, 2001]. The model used 2 loops, one for the broken loop, and the other representing the 3 intact loops. The steam lines were modeled and connected to a common steam header. The main steam isolation valves were modeled, with the break located upstream of the broken loop MSIV. A homogeneous core model was used, and the fuel was modeled with an average rod and a hot rod. This modeling approach is typical for full power conditions because the high flow at full power promotes effective mixing in the lower plenum. For both the solid and annular geometries, a hot assembly was not explicitly modeled. Essentially, it was assumed that the outer surfaces of the hot rod and average rod would see similar fluid conditions.

Table 7.6 MSLB at Power - Initial Condition

Parameter	Solid (100%)	Annular (100%)	Annular (150%)
Reactor Power, MW	3473.1	3479.1	5218.4
Primary Pressure, MPa	15.52	15.52	15.52
Secondary Pressure, MPa	6.91	6.92	6.92
Loop Flow (all 4 loops),kg/s	17244	17407	26809
Core Flow, Kg/s	17000	17172	26449
Cold Leg Temperature, °K	569.4	569.5	569.5
Hot Leg Temperature, °K	603.6	603.4	602.7
Feedwater Flow (Total),kg/s	1930	1912	2869
Core Pressure Drop, kPa	178	154.8	312.8

The initial fuel conditions correspond to end-of-cycle conditions. The initial system parameters were conservatively assumed, as in typical licensing analyses. The system transient behavior is described by plots of the core flow rate, the core inlet fluid temperature, the pressure at the core outlet, and the total reactor power. Each of these plots show the solid rod behavior compared to the annular rod behavior at power levels of 100% and 150%. Table 7.6 below shows the initial conditions for the three cases. For 150% power, it was assumed by design that the initial loop flow rates would be 50% higher than at 100% power. The intent was to maintain approximately the same primary fluid temperatures as the 100% power case. The steam generators (and steam lines) were correspondingly larger, with flow areas and volumes enhanced by 50%, and the break size used for the evaluations at 150% power was 50% higher than for 100% power.

Table 7.7 shows the sequence of events for the cases analyzed, for the large steam line break. In all cases, the reactor trip was caused by overpower, and the sequence of events were qualitatively similar.

The calculation results are shown in Figures 7.7 through 7.10. Figure 7.7 shows a comparison of the transient core inlet mass flow rate. For all cases, the core flow

increases slightly in the initial phase of the accident because the reactor coolant pumps are still running at full speed, and the temperature is steadily decreasing. The core inlet temperature transient is shown in Figure 7.8, showing the decreasing temperature in this initial phase. The system pressure decreases correspondingly as seen in Figure 7.9. The resulting transient reactor power increase due to reactivity insertion is shown in Figure 7.10. The reactor power increase is accompanied by an increase in the temperature difference across the core. Generally, the reactor protection system logic uses a combination of the power and ΔT across the core to provide the Overpower ΔT trip. Here, the logic was simplified, to trip the reactor when the core ΔT increased to 112% or the reactor power increased to 110%. While the reactor power is increasing, the steam line pressure is steadily decreasing. This can also cause a reactor trip.

Table 7.7 Sequence of Events for MSLB Case 1, 0.013 m², at times in seconds

Trip Function	Solid 100%	Annular 100%	Annular 150%
Accident initiation	0.0	0.0	0.0
Rx Trip Signal on 110% Overpower	26.9	23.375	18.4625
Rx Trip signal	26.9	23.375	18.4625
Rx trip (with rod drop delay)	28.9	25.375	20.4625
Lo Pressurizer Pressure (1945.7 psia)	33.0	29.8	23.225
Lo-Lo Pressurizer Pressure (1866.7 psia)	35.05	31.15	24.85
SI Signal, ECCS Available	35.05	31.15	24.85
Isolate Feedwater (SI Signal + valve delay)	41.05	37.15	30.85
Low BL MSL Pressure (582.7 psia)	42.05	37.45	31.55
Charging On (SI Signal + delivery delay)	47.05	43.15	36.85
BL MSIV closed (low pressure + valve closing)	48.05	43.45	37.55
Low IL MSL Pr (582.7 psia)	54.9	50.5	41.45
IL MSIV closed (low pressure + valve closing)	60.9	56.5	47.45
ECCS Injection (SI signal + delivery delay)	62.05	58.15	51.85

Figure 7.7 Core flow rates during MSLB Transients

Figure 7.8 Core inlet temperatures during MSLB Transients

Figure 7.9 Upper plenum pressures during MSLB Transients

Figure 7.10 Reactor power during MSLB Transients

Subsequent to the reactor trip, the core inlet temperature and system pressure decrease more rapidly, causing a proportional increase in the core inlet mass flow rate. This phase ends when the intact loop SGs are bottled up (MSIV closure), and the broken loop steam generator dries out, causing a slow temperature and pressure increase in the primary. Correspondingly, the core inlet mass flow rate decreases slightly. In any case, at this point, the reactor has been tripped, and secondary heat removal is via the safety valves on

the intact SGs. Emergency feedwater is available, the main steam bypass is available, and subsequently the operator has several options to achieve a controlled state.

7.4.2 MDNBR (VIPRE) Calculations

The above results of the RELAP5 analysis were used in a sub-channel analysis using the VIPRE code, which calculates the DNBR. The transient input parameters provided from the RELAP5 analysis to VIPRE were the reactor power, the core inlet flow rate and temperature, and the core outlet pressure. For the annular geometry, the data provided included the core flow rate through the inner and outer channels of the fuel rods. The core peaking factors and power distributions were maintained fairly close between the two codes.

Figure 7.11 shows the resulting DNB ratio for the 0.13 m² (1.4 ft²) break size, for the solid rods at 100% power, compared to the annular rods at 100% power and at 150% power. For the annular rods, DNB ratios at both the inner and outer surfaces are shown. For both cases, the annular rods have higher DNB ratios than the solid rods. The DNBR for the annular fuel is more sensitive than for the solid fuel, and decreases as the reactor power increases, but does not decrease below the value for the solid rod.

Figure 7.11 MDNBR for MSLB Transients

7.5 Large Break Loss of Coolant Accident (LBLOCA)

7.5.1 Assumptions and Boundary Conditions

For all three cases, i.e., the solid fuel – 100% power, annular fuel – 100% power and the uprated annular fuel – 150% power), a double-ended cold leg break was modeled by opening two break junctions to containment from the outlet of volume 212 and inlet of

volume 214, as shown in Figure 7.2. The containment back-pressure, initially at 1 bar, was modeled to vary as shown in Figure 7.12 during the transient. This pressure represents 90% of the value of the containment minimum pressure used in the reference Final Safety Analysis Report (FSAR) of the reference PWR for the double-ended cold leg break.

The emergency core cooling (ECC) safety injections from the accumulator and the pumped system were modeled. For conservatism, no credit was taken for the safety injection into the broken loop; thus the safety injections were provided only to the intact loops. Conservative boundary conditions as shown in Table 7.8 and Figure 7.13 based on the reference FSAR and USNRC model were used for the 100% power cases for both the solid and the annular fuels.

Figure 7.12 Containment Back Pressure used during LBLOCA Analyses

Table 7.8 Safety Systems Modeling

Systems	Models
Accumulator	
- Initial Pressure	41.37 bars
- Initial Temperature	37.8 °C
- Initial Liquid Volume	23.12 m ³ /Acc
- Transient Flow	dynamic Model
Pumped Injection	
- Temperature	37.8 °C
- Transient Flow	conservative boundary

For the case of uprated annular fuel (150% power), several options were evaluated to identify the optimum accumulator size and the SI flow rate that can accommodate 50% higher power. Major options calculated include:

1. 150% accumulator and 100% SI
2. 100% accumulator and 150% SI
3. 150% accumulator and 150% SI

Figure 7.13 Pumped Safety Injection Flow Models

The parameters used for transient modeling of the plant protection and control systems are listed in Table 7.9. These data were selected from typical PWR data. A locked rotor condition was conservatively assumed for the reactor coolant pumps at the initiation of the break.

Table 7.9 Parameters for Transient Modeling of Plant Protection and Control Systems

Systems	Setpoint	Delay (sec)
Reactor Trip Signal – Low PZR P	134.15 bar	2.0
Turbine Trip Signal – Rx Trip	134.15 bar	2.0
TBN Stop Valve Closing Time	0.2 sec	
Main Feedwater Trip Signal – Rx Trip	134.15 bar	2.0
Main Feedwater Valve Closing Time	5.0 sec	
Safety Injection Signal – Low PZR P	128.7 bar	27.0
Aux. Feedwater Actuation – SI	128.7 bar	10.0
<i>Reactor Coolant Pump Locked Rotor</i>	<i>LBLOCA</i>	<i>0.0</i>

PZR: Pressurizer, Rx: Reactor, TBN: Turbine

Additional modifications to the original USNRC model involve the following:

- Hydraulic diameters for junction inter-phase drag calculations
- Rod bundle inter-phase friction calculations at core and steam generators
- Use of piping roughness based on plant data:
 - Reactor vessel and reactor coolant system piping: 10^{-4} (m)
 - Fuel and steam generator tube outside: 10^{-6} (m)
 - Steam generator tube inside: 1.5×10^{-6} (m)
- Cross-flow at cold and hot leg junctions to and from reactor vessel,
- A common flow header in the safety injection line upstream of the cold leg in order to prevent sudden injection of accumulator non-condensable gas into the system.

The LBLOCA transient was initiated at time zero. Henry-Fauske critical flow model was used for the flow through the break. The reflood model with fine-mesh rezoning scheme was turned on when the core pressure decreased below 10 bars.

7.5.2 Comparison and Discussion of LBLOCA Results

The results of the best estimate LBLOCA analysis using RELAP5-3D [2003] for the reference solid fuel for 100% power are presented in Figures 7.14 through 7.17. Figure 7.14 shows the solid fuel cladding temperatures at 6 different axial nodes of the hot pin. Figure 7.15 shows the flow through the break, Figure 7.16 depicts water levels in the reactor core and the downcomer, and finally Figure 7.17 shows the accumulator and pumped safety injection flow rates as a function of time. It can be observed that the blowdown peak cladding temperature (PCT) and reflood PCT {1000K (2.15 sec, node 9/16) and 1048K (124.5 sec, node 11/16), respectively}, have sufficient margin to the regulatory limit of 1473K. Reasonable cladding temperature development can be observed showing typical blowdown peak temperature, more gradual cladding heat up, then quenching. Also, the development of all other plotted parameters proceeds in time as expected. The results of the reference LBLOCA analysis will serve as a base case for comparison with the LBLOCA behavior of the core having annular fuel.

Figure 7.14 Hot Pin Cladding Temperature during LBLOCA (Solid Fuel – 100% Power)

Figure 7.15 Break Flow Rate during LBLOCA (Solid Fuel – 100% Power)

Figure 7.16 Core and Downcomer Water Levels during LBLOCA (Solid Fuel – 100% Power)

Figure 7.17 Safety Injection Flow Rates during LBLOCA (Solid Fuel – 100% Power)

We will now examine the LBLOCA results for the annular fuel, particularly for the uprated 150% power condition. As mentioned in Section 7.5.1, various options regarding the accumulator size and pumped SI flow were considered for the uprated condition. Both inner and outer peak cladding surface temperature traces of various cases for 150% power with annular fuel, with reflood model applied to the outer channel, are plotted in Figure 7.18.

Figure 7.18 Peak Cladding Surface Temperatures (both inner and outer) for Annular Fuel (150% Power) with various Accumulator Sizes and Pumped SI Flow Rates

It can be seen from Figure 7.18 that the inner and outer cladding surface temperature traces are closely matched. The annular fuel has thin fuel thickness so that the temperature difference between the inner and outer surfaces is small. It is also shown in the figure that the blowdown peak temperatures are nearly equal for all the cases. After around 50 seconds, the temperature of the case with 100% accumulator reaches a little higher value than those of the other two cases with 150% accumulator. This is because more cold coolant from the larger accumulators depresses the temperature rises. However, the effect of larger accumulator lasts only for a short time and does not influence the reflood PCT. As expected, the pumped SI flow rate plays an important role in the cladding temperature behavior during the reflood phase of the accident. Even though the peak temperatures are below the safety limit of 1473K (1200°C) for all three cases, it can be observed that the case with 100% SI yields higher reflood peak temperature, and it lasts at high values much longer before quenching. As for the two cases with 150% SI, they have almost the same PCT, except that the case with 150% accumulator size quenches a little earlier than the case with 100% accumulator size. Both of them are regarded acceptable. With consideration for cost saving, 100% accumulator size is more desirable. Therefore, SI at 150% capacity and the same accumulator size (100% capacity) were selected for the 150% rated core loaded with the annular fuel.

Peak cladding temperatures for the reference solid fuel, annular fuel at 100% power and annular fuel at 150% power with 150% SI and 100% accumulator capacity are compared in Figure 7.19. It can be seen that although the annular fuel at 150% power produces higher peak cladding temperature than the reference solid fuel at 100% power, it remains well below the 1470 °K limit. It should be noted that the RELAP5-3D does not have the capability of applying the reflood model to both “left” and “right” boundaries (in annular fuel, these two boundaries refer to the inner and outer cooling surfaces of the annular fuel rod) of a heat structure. It only has the capability of applying the reflood model to one

boundary. In the calculation results shown above, the reflood model is applied to the outer cooling surface. The results of the calculations with the reflood model applied to the inner cooling surface only also is shown for comparison for the 150% annular fuel case in Figure 7.19 . The peak cladding temperature calculated by RELAP-3D with reflood model applied to the inner channel is lower by about 100 °C than in case of its application to the outer channel. Since the temperatures of the inner and outer surfaces are very close, it is expected that the actual temperatures in case of reflood model on both surfaces, the temperatures will follow the trend for the inner reflooding, i.e., temperature of outer channel will be close to that of the inner channel that floods first. The reason for earlier reflooding of the inner channel is the absence of local pressure losses of the grids and its larger hydraulic diameter. Further calculations with reflood model applied in both the inner and outer channels are needed to confirm this expectation. This will require RELAP5-3D reflood model update.

As expected, the annular fuel at 100% power yields lower peak cladding temperature than the solid fuel at 100% power. It can be also noted that the first peak cladding temperature during the blowdown phase no longer exists for the annular fuel, even at 150% power. This is due to the very low annular fuel temperature, and thus the stored energy.

Figure 7.19 Comparison of Peak Cladding Surface Temperatures for Solid Fuel at 100% Power and Annular Fuel at 100% and 150% Powers

Selected important LBLOCA results for annular fuel at 150% power with 100% accumulator and 150% Safety Injection case with reflood model applied to the outer channels are shown in Figures 7.20 through 7.24. Figure 7.20 plots the safety system flow rates, where flow of safety injection is 150% of that of reference value. SI flow into the broken loop is not taken into credit, so zero values are shown in the figure. Figure 7.21 depicts collapsed water levels in the average core and the hot assembly, broken and intact

downcomers. Figure 7.22 shows coolant inventory in primary system and the reactor vessel. After the initial period of about 30 seconds, most of the primary coolant is calculated to be in the reactor vessel. Figure 7.23 and Figure 7.24 plot the inner and outer cladding surface temperatures of the hot rod at various locations. It can be seen that the inner and outer cladding surface temperatures are very close during the LBLOCA transient for the case of annular fuel at 150% power condition.

Figure 7.20 Safety Systems Flow Rates for Annular Fuel (150% power, 100% Acc., 150% SI Flow Rate)

Figure 7.21 Collapsed Water Level in Reactor Vessel for Annular Fuel (150% power, 100% Acc., 150% SI Flow Rate)

Figure 7.22 Primary System Coolant Inventory during LBLOCA for Annular Fuel (150% power, 100% Acc., 150% SI Flow Rate)

Figure 7.23 Inner Cladding Surface Temperatures of the Hot Annular Fuel Rod during LBLOCA (150% power, 100% Acc., 150% SI Flow Rate)

Figure 7.24 Outer Cladding Surface Temperatures of the Hot Annular Fuel Rod during LBLOCA (150% power, 100% Acc., 150% SI Flow Rate)

7.6 Rod Ejection Accident (REA)

Since REA causes sudden insertion of positive reactivity in a particular region of the reactor core, an appropriate method of analysis is to use a coupled, three-dimensional, neutronic-thermal hydraulic calculation to determine the localized power response. This is then combined with a fuel rod thermal analysis to determine the energy deposition and fuel centerline temperature during the rapid rod ejection transient. A simplified approach is typically used in the current licensing analysis, which is derived from the more rigorous three-dimensional analysis, and benchmarked for a typical Westinghouse 4-loop 3411 MWt PWR. The simplified method uses point kinetics calculations, with key parameters (Doppler coefficient and peaking factors) adjusted to account for the difference from the three-dimensional method. This is the approach followed here to assess the performance of the annular fuel with respect to this accident. Hence, some approximations have been made in this comparative study.

7.6.1 Neutronic Calculations

Neutronics calculations were carried out first to determine the most reactive rod at two initial conditions, namely, Hot Zero Power (HZIP) and Hot Full Power (HFP), for the solid at 100% power level and the annular fuel, at both 100% and 150% power levels. For each case, a static calculation was conducted with SIMULATE [Cronin et al., 1995], where the most reactive rod was ejected. This provided static post-ejection peaking factors. Table 7.10 shows the results, for solid and annular fuel rod geometries at HZIP and HFP conditions, for 100% power operation and for 150% power operation. Also shown for each case are the ejected rod worth and the fuel Doppler coefficient, which is an important parameter for this event.

Table 7.10 Post-Ejection Physics Parameters

Core type	Solid	Annular	Annular, 150%
EOC, HZP			
Maximum Rod Worth	274.0 pcm 52.3 cents	130.4 pcm 24.6 cents	110.5 pcm 20.3 cents
Delayed Neutron Fr, β_{eff}	0.005235	0.005294	0.005443
Radial Peaking, $F_{\Delta h}$	3.648	2.549	2.120
Total Peaking, F_q	6.763	5.792	5.831
Doppler Coefficient, pcm/ °C	-1.61	-1.81	-1.76
EOC, HFP			
Maximum Rod Worth	27.6 pcm 5.32 cents	19.8 pcm 3.77 cents	18.1 pcm 3.33 cents
Delayed Neutron Fr, β_{eff}	0.005190	0.005256	0.005435
Radial Peaking, $F_{\Delta h}$	1.576	1.563	1.514
Total Peaking, F_q	2.243	2.065	2.120
Doppler Coefficient, pcm/ °C	-1.89	-1.91	-1.94

7.6.2 System (RELAP5) Calculations

A system thermal-hydraulic calculation has been carried out with RELAP5 for HFP, modeling an average fuel rod and a hot rod. The hot rod is initially assumed to have the post-ejected peaking factors shown in Table 7.10. After that, the transient is initiated by inserting the positive reactivity shown in Table 7.10 for each case, over 0.05 seconds. The core power increases, the reactor trip signal occurs on high neutron flux, the initial power spike is mitigated by the fuel Doppler coefficient, the other control rods are inserted, and the power finally decreases as the reactor is shut down.

In the current analysis, it is recognized that the SIMULATE calculations were conducted starting from a realistic initial condition, where the initial peaking factors are lower than assumed in typical licensing calculations. The RELAP5 calculations of the MSLB event discussed in Section 7.4 used a radial peaking factor of about 1.62 and a total peaking factor of about 2.5. The SIMULATE calculations used more realistic initial values. Based on this, and on the adjustment factors discussed above, the peaking factors used for the annular fuel were assumed to be similar to those for solid rods. Initial calculations showed very benign results for the annular rods. Hence, it was assumed that for all the calculations for a given initial condition (HFP), the ejected rod worth was assumed to be 0.2\$. Note from Table 7.10 that both the post-ejected peaking factors and the ejected rod worths are consistently lower than those for the solid rods. Hence these are conservative assumptions for this comparative study.

The results are shown in terms of the fuel temperatures. Figure 7.25 shows the maximum fuel temperatures, for HFP conditions. The transient is initiated at 1 second. The maximum fuel temperature for the solid rod is at the fuel centerline. For the annular rods, it is taken at an interior node, biased closer to the center. It can be seen that the maximum fuel temperatures for the annular geometry are significantly lower than that for the solid rod. The initial power increase was similar (on a relative basis) for all the cases, because the ejected rod worth was assumed similar. Hence the difference is mainly due to the

increased heat transfer area available for the annular rods. The large margin available indicates that the annular rods should be able to successfully withstand the rod ejection event even at 150% power level. **REA, HFP, ERW=0.2\$**

Figure 7.25 Maximum Fuel Temperature Comparisons for REA at HFP

Figure 7.26 Average Fuel Temperature Comparisons for REA at HFP

Figure 7.26 shows the average temperature and enthalpy in the fuel. The trend is the same as for the maximum temperature. The energy deposited in the fuel rod (calories/gram) can be calculated from the average fuel temperature. Because the average fuel temperatures for the annular rods are significantly less than that for solid rods, and margin to enthalpy limit of 280 calories/gram is much larger for the annular fuel, it can be concluded that the annular fuel will perform better during REA than the solid fuel. Moreover, the large margin of annular fuel to energy deposition limits provides better potential to meet the reduced energy deposition limit for high burnup fuels, once this limit is established.

As shown in Table 7.10, the ejected rod worths for HZP are considerably higher than those for HFP. On the other hand, the initial fuel temperatures would be significantly lower. The high neutron flux setpoint is also lowered at HZP (25% compared to 109%). Generally, HZP initial conditions provide more limiting calculation for the rod ejection event. However, considering the results shown in Figures 7.25 and 7.26 for the HFP case, and the data in Table 7.10 (lower rod worths, higher Doppler coefficients for annular rods), the results for HZP should be very similar to those for HFP, i.e., the annular rod performance, even at 50% higher power, would be better than that of solid rods.

7.7 Summary and Conclusions

Safety analyses for a typical Westinghouse 4-loop PWR were carried out for the reference solid fuel at 100% power, the annular fuel at 100% power and the annular fuel at 150% power. Four representative transients, namely, the Loss of Flow Accident (LOFA), Main Steam Line Break (MSLB), Large Break Loss of Coolant Accident (LBLOCA) and Rod Ejection Accident (REA) were run using slightly different versions of the RELAP5 code. However, the same version of the code was used for a particular transient for both the solid and annular fuels. Hence, the differences in results for a particular transient reflect the differences due to the fuel type – solid vs. annular – and due to the differences in power – 100% vs. 150% for the annular fuel.

For the cases of LOFA and MSLB, the VIPRE code was used to calculate the minimum DNBR based on the RELAP5 results. It has been found that the minimum DNBR for the annular fuel at 150% power never dropped below the minimum DNBR value for the reference solid fuel at 100% power for LOFA and MSLB. This is a significant achievement for the annular fuel over the standard solid fuel.

For LBLOCA analysis with annular fuel at 150% power, various options for the accumulator sizes and the safety injection flow rates were studied. It has been found that for LBLOCA with reflood model applied to the outer channels, the peak cladding temperature (PCT) for the annular fuel at 150% power would be around 1200° K (927°C) using the standard size (100%) accumulator but increasing the safety injection flow rate to 150%. This temperature (927°C) is about 150°C higher than the PCT for the solid fuel at 100% power, but about 277°C lower than the regulatory limit of 1204°C. The reflood PCT for the annular fuel may be over-predicted because of the limited capability of RELAP5-3D reflood model, which is only applied on the outer surface of the annular fuel rod. If the reflood model is applied to the inner channels, the peak cladding temperature

would reach the value of 1100°K, which is only 50 °C higher than that of the solid fuel at 100% power. Since the temperatures of the inner and outer surfaces are very close, it is expected that the actual temperatures with reflood model turned on both surfaces, the temperatures will follow the trend for the inner reflooding, i.e., the temperature of the outer channel will be close to that of the inner channel, which floods first. There is a need for developing a more powerful safety analysis code that has the capability to apply reflood models to both “left” and “right” boundaries for heat structures so that annular fuel can be better modeled in LBLOCA analyses, especially during the reflood phase.

The calculated fuel temperatures during the REA have also been found to be much smaller for the annular fuel, even at 150% power, compared to that for the solid fuel at 100% power. This is not surprising since the fuel temperature and the stored energy for the annular fuel at 150% power are significantly less than that for the solid fuel at 100% power.

In conclusion, the analyses indicate that the new internally and externally cooled annular fuel can accommodate 50% power uprate in a PWR and still maintain adequate safety margins for a variety of transients and accidents including LOFA, MSLB, LBLOCA and REA.

8. MECHANICAL ANALYSIS

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The annular fuel is subjected to both axial and cross coolant flows externally and to axial coolant flow internally and its resistance to vibrations needs to be verified, especially in view of significantly higher velocities at 150% power than for the reference core at 100% power output. These issues are addressed in this Chapter.

The gas gap between the cladding and the fuel material will initially be open, which is referred to as open gap case. In this case, the outer cladding and inner cladding vibrate separately from the pellet. As the fuel is irradiated, the cladding and fuel will contact each other to form a single body, which is named the closed gap case. This is also the case for Vibration PACKed (VIPAC) fuel from microscale particles filled directly into the cladding. These two cases have been addressed separately.

Five flow-induced vibration mechanisms are addressed here: Buckling instability, Vortex-induced vibration, Acoustic resonance, Fluid-elastic instability, and Turbulence-induced vibration.

Buckling instability is a static instability by bending. Since the internal cladding is a very long hollow tube, buckling due to internal flow in the annular fuel produces stresses by a bending moment.

Periodic wake shedding occurs immediately downstream of structures subjected to cross flow. Periodic wake shedding generates periodic fluid forces. When the structural natural frequency is close enough to the vortex-shedding frequency, a phenomenon known as lock-in occurs whereby the shedding frequency coincides with the structural natural frequency. As a result, resonant vibration by lock-in can potentially lead to fuel failure.

Acoustic pulsations, such as those generated by coolant pumps, can potentially cause large responses if standing pressure waves are produced in the coolant channels. Standing waves are of non-propagating nature and result from a reflected wave that is superimposed onto the original wave, forming interference patterns which have amplified deflections. One of the most detrimental acoustic problems for the fuel is induced when one or more of the lower acoustic modes are excited by vortex-shedding in the fuel bundle. The acoustic cavity resonance correlated with shedding causes intense acoustic noise, which can lead to severe structural damage.

Fluid-elastic instability is one of the main mechanical damage mechanisms and should be absolutely avoided. Fluid-elastic instability produces excessive vibration amplitudes by negative damping resulting from coupling between fluid-induced dynamic forces and the motion of the structures. Instability occurs when the flow velocity is sufficiently high so that the energy absorbed from the fluid forces exceeds the energy dissipated by damping. The minimum velocity at which instability occurs is called the critical velocity for fluid-elastic instability.

Turbulence-induced excitation generates random pressure fluctuations around the fuel surfaces forcing them to vibrate. Turbulence-induced excitation is the primary vibration excitation mechanism in axial flow. Turbulence-induced excitation may cause sufficient vibration response to cause long-term fretting-wear damage. The life-time wear of the fuel cladding should be less than a critical wear, at which cladding failure may be expected.

8.1 Calculation of Dynamic Parameters

First the properties and parameters needed in the calculation will be defined for both the solid fuel and the annular fuel. Then, the potential design issues will be assessed. Zircaloy-4 and UO₂ are the materials of cladding and fuel, respectively, whose properties are obtained from [Hagman et al, 1980].

8.1.1 Flexural Rigidity and Linear Mass

1) Young's modulus and density for the Zircaloy-4 cladding and the UO₂ fuel:

Density of cladding: $\rho_{cl} = 8470 \text{ kg/m}^3$

Fuel theoretical density: $\rho_f = 10421 \text{ kg/m}^3$

$$E_{cl} = (108800 - 54.75T_{cave} - 5200) \times 10^6 / 0.88 \quad (8-1)$$

where

E_{cl} : cladding Young's modulus in MPa

T_{cave} : cladding average temperature in K

$$E_f = (1.8863 \times 10^{11} - 9.0627 \times 10^7 T_f) \times (2.28f_{raden} - 1.28) \quad (8-2)$$

where

E_f : fuel Young's modulus in MPa

T_f : fuel temperature in K

f_{raden} : fractional fuel density (ratio of actual density to theoretical density)

2) Linear mass

(1) Linear mass for solid fuel (17x17 rectangular lattice fuel)

$$\text{linear mass of cladding: } M_{clo} = (\rho A)_{clo} = \rho_{cl} (\pi / 4) (D_{clo}^2 - D_{cli}^2) \quad (8-3.a)$$

$$\text{linear fluid mass: } M_{hy} = C_M (\pi / 4) \rho_{flo} D_{clo}^2 \quad (8-3.b)$$

where C_M stands for added mass coefficient defined as

$$C_M = (D^{*2} + 1) / (D^{*2} - 1)$$

$$D^* = (1 + P / 2D)(P / D)$$

D: outer diameter of fuel pin

P: pitch of fuel lattice

subscripts, cl, clo and cli, and flo represent cladding, outer and inner surfaces of cladding, and fluid in the external channel.

$$\text{The total linear mass is given by: } M_t = M_{cl} + M_{hy} \quad (8-4)$$

(2) *Linear mass for annular fuel in a rectangular lattice*

$$\text{linear mass of inner cladding: } M_{cli} = (\rho A)_{cli} = \rho_{cl} (\pi / 4) (D_{clio}^2 - D_{clii}^2) \quad (8-5)$$

$$\text{linear mass of outer cladding: } M_{clo} = (\rho A)_{clo} = \rho_{cl} (\pi / 4) (D_{cloo}^2 - D_{cloi}^2) \quad (8-6)$$

$$\text{linear fluid mass of outer coolant: } M_{hyo} = C_M (\pi / 4) \rho_{flo} D_{cloo}^2 \quad (8-7)$$

$$\text{linear fluid mass of inner coolant: } M_{hyi} = (\pi / 4) \rho_{fli} D_{clii}^2 \quad (8-8)$$

$$\text{total linear mass for outer cladding: } M_{to} = M_{clo} + M_{hyo} \quad (8-9)$$

$$\text{total linear mass for inner cladding: } M_{ti} = M_{cli} + M_{hyi} \quad (8-10)$$

where

subscripts, clo and cli represent the outer and inner claddings,
subscripts clio,clii,cloo, and cloi represent outer and inner surfaces of the inner and outer claddings,
subscripts, flo and fli represent the fluids in the external and internal channels.

8.1.2. Moment of Inertia

1) Moment of inertia for solid fuel (17x17 fuel)

$$\text{Moment of inertia of cladding: } I_{cl} = (\pi/64)(D_{clo}^4 - D_{cli}^4) \quad (8-11)$$

2) Moment of inertia for annular fuel

$$\text{Moment of inertia of inner cladding: } I_{cli} = (\pi/64)(D_{clio}^4 - D_{clii}^4) \quad (8-12)$$

$$\text{Moment of inertia of outer cladding: } I_{clo} = (\pi/64)(D_{cloo}^4 - D_{cloi}^4) \quad (8-13)$$

8.1.3 Natural Frequency and Mode Shape Function

For a multi-span beam with both ends of each span simply supported, the natural frequency is given by [Au-Yang, 2001]:

$$f_n = (n\pi)^2 / [2\pi(L/m)^2] \sqrt{EI / M_t} \quad (8-14)$$

And the amplitude is given by

$$\delta = \sum_{n=1}^N \psi_n(x)$$

where

$$\psi_n(x) = \sqrt{2/L} \sin[n\pi x/(L/m)]$$

n : modal number, equals to 1,2,3

m : the number of the spans

L : total length of the tube

M_t : total linear mass including fluid and solid

8.1.4. Damping Ratio

The damping ratios for tubes in water for various water densities and support types are listed in Table 8.1 [Au-Yang, 2001].

Table 8.1: Damping ratios in water (ζ)

	ζ , water or wet steam, tightly supported tube	ζ , water or wet steam, loosely supported tube	ζ , two-phase, loosely supported tube
Mean value	0.015	0.05	0.05*
Conservative value	0.01	0.03	0.03*

* This value is based on Au-Yang's experience [Au-Yang, 2001]

8.1.5. Sample Calculation on Physical properties

The physical characteristics of interest for both the case of the annular fuel design (15x15 and 13x13) and the reference PWR (17x17) assembly are given in Tables 8.2 through 8.5.

Table 8.2: Geometric data of the solid fuel and the annular fuel (mm)

Fuel type	D_{cii}	D_{cio}	D_{fi}	D_{fo}	D_{coi}	D_{coo}	Pitch	P/D
15x15	6.73	7.87	8.0	11.978	12.102	13.245	14.31	13.26
13x13	8.633	9.776	9.9	14.1	14.224	15.367	16.51	10.7
17x17 ref	N/A	N/A	N/A	8.255	8.379	9.522	12.63	10.8

Table 8.3: Estimation of linear mass (Kg/m)

Fuel type	C_m	M_{hyo}	M_{clo}	M_{cli}	M_{to}	M_{hyi}	M_{ti}	M_f	M_t
15x15	2.131	0.217	0.193	0.111	0.41	0.026	0.137	0.65	1.198
13x13	2.158	0.296	0.225	0.140	0.521	0.043	0.183	0.825	1.53
17x17 ref	1.517	0.08	0.136	N/A	0.216	N/A	N/A	0.56	0.774

Table 8.4: Estimation of Young's modulus(MPa) and moment of inertia(Kg.m²)

Fuel type	I _{cli}	I _{clo}	E _{cl}	I _t	E _t
15x15	8.76E-11	4.58E-10	1E+11	1.34E-9	1E+11
13x13	1.76E-10	7.28E-10	1E+11	2.36E-9	1E+11
17x17 ref	N/A	1.62E-10	1E+11	3.76E-10	1E+11

Table 8.5: Estimation of natural frequency(Hz)

Fuel type	span length (m)	f _{1cli} [*]	f _{2cli} [*]	f _{1clo} [*]	f _{2clo} [*]	f _{1t} [*]
15x15	0.508	48.43	193.74	64.0	256.0	64.5
13X13	0.508	59.31	237.23	71.59	286.35	75.6
17x17 ref	0.508	N/A	N/A	52.38	209.5	42.4

* Note that subscripts, 1 and 2, in Table 8.5 represent the first and second modes of the natural frequency, and subscripts, cli and clo, represent the natural frequency of the inner and outer claddings in case of open gaps, while subscript t represents the closed gaps case.

8.2 Buckling Instability of Annular Fuel

8.2.1 Basic Theory

For annular fuel, the coolant will flow through the inner channel as well as through the external coolant channel. Let us assume that the inner cladding undertakes a slight lateral displacement $y(x)$. Five kinds of forces will be involved in the mechanical equilibrium.

These are:

Inertial force: $(M_{cl} + M_{fi})\partial^2 y / \partial t^2$;

Elastic force: $EI\partial^4 y / \partial x^4$;

Fluid pressure force: $(p_{fi} - p_g)A_{fi}\partial^2 y / \partial x^2$;

Centrifugal force: $\rho_{fi}A_{fi}V_{fi}^2\partial^2 y / \partial x^2$;

Coriolis force: $2\rho_{fi}A_{fi}V_{fi}\partial^2 y / \partial x \partial t$.

where

E : Young's module of inner cladding

I : geometric area moment of inertia of inner cladding

x : distance from one end of the tube
 M_{cl} : linear mass density of inner cladding
 M_{fi} : linear mass density of inner coolant
 p_{fi} : pressure of inner coolant
 p_g : pressure of gap gas between cladding and pellet
 ρ_{fi} : density of inner coolant
 A_{fi} : cross section area of inner channel
 V_{fi} : velocity of inner coolant

Forming the force equilibrium yields the following equation:

$$(M_{cl} + M_{fi})\partial^2 y / \partial t^2 = -[EI\partial^4 y / \partial x^4 + (p_{fi} - p_g)A_{fi}\partial^2 y / \partial x^2 + \rho_{fi}A_{fi}V_{fi}^2\partial^2 y / \partial x^2 + 2\rho_{fi}A_{fi}V_{fi}\partial^2 y / \partial x \partial t] \quad (8-15)$$

At steady state, Eq. (8-15) becomes:

$$EI\partial^4 y / \partial x^4 + (p_{fi} - p_g)A_{fi}\partial^2 y / \partial x^2 + \rho_{fi}A_{fi}V_{fi}^2\partial^2 y / \partial x^2 = 0 \quad (8-16)$$

When destabilizing forces such as the centrifugal force are higher than the stabilizing force such as the elastic force due to the stiffness of the tube, the pipe deflects more, which causes the buckling instability. The velocity at which the destabilizing forces equal the stabilizing forces is called the critical velocity for buckling instability.

For a simple-supported tube at both ends, the critical velocity for the fundamental model is given as

$$V_c = [\pi^2 EI / (L^2 M_{fi}) - (p_{fi} - p_g) / \rho_{fi}]^{1/2} \quad (8-17)$$

Note that the internal coolant pressure contributes to reduction of the critical velocity. The internal pressure is at minimum at the beginning of irradiation. The critical velocity could become zero if the second term under the square root in the right equals or exceeds the first term.

8.2.2 Consideration of the Inner Cladding of Annular Fuel (15x15)

Here, we only consider the freely oscillating inner cladding simply supported at both ends and decoupled from the fuel and the outer cladding with an internal cold gas pressure.

1) Calculation of the critical velocity in the annular fuel

For conservatism, data at the room temperature are used:

$$p_{fi} = 15.2 \times 10^6 \text{ Pa}, \quad p_g = 2 \times 10^6 \text{ Pa}$$

$$\delta_{gi} = \delta_{go} = 0.62 \times 10^{-4} \text{ m}$$

where

δ_{gi}, δ_{go} : gas gap thickness between cladding and pellet (inner and outer)

ρ_{cl}, ρ_{fi} : cladding and fluid densities = 8740, 1000 kg/m³

$M_{cl} = (A\rho)_{cl} = 0.3013 \text{ kg / m}, M_{fi} = (A\rho)_{fi} = 0.0356 \text{ kg / m}$

$I = 8.76 \times 10^{-11} \text{ m}^4$

$E = 9.9 \times 10^{10} \text{ Pa}$

thus,

$$\pi^2 EI / (L^2 m_{fi}) = \pi^2 9.9 \times 10^{10} \times 8.76 \times 10^{-11} / [3.66^2 \times 0.0356] = 179$$

$$(p_{fi} - p_g) / \rho_{fi} = (15.2 - 2.0) \times 10^6 / 1000 = 13200$$

From Eq. (8-17), one can observe that the critical velocity becomes zero as $(p_{fi} - p_g) / \rho_{fi} \geq \pi^2 EI / (L^2 m_{fi})$. Hence, buckling instability may take place in the annular fuel. It is possible that the center of the inner cladding would be bent up towards the sintered fuel.

2) Calculation of the maximum stress due to the bending moment

When the buckling instability takes place, the inner cladding will push the pellet moving it out to contact with outer cladding. Thus, the maximum vibration amplitude of the tube at its mid-span will be $\delta = \delta_{gi} + \delta_{go} = 1.24 \times 10^{-4} \text{ m}$. Using a harmonic oscillation with a given maximum vibration amplitude yields the following relationship:

$$\begin{aligned} y &= \delta \sin \pi x / L \\ \partial y / \partial x &= (\pi \delta / L) \cos \pi x / L \\ \partial^2 y / \partial x^2 &= -(\pi / L)^2 \delta \sin \pi x / L \\ \partial^3 y / \partial x^3 &= -(\pi / L)^3 \delta \cos \pi x / L \\ \partial^4 y / \partial x^4 &= (\pi / L)^4 \delta \sin \pi x / L \end{aligned} \quad (8-18)$$

Utilizing the above relationship, the bending moment in a vibrating structure becomes:

$$M = -EI \partial^2 y / \partial x^2 = -EI (\pi / L)^2 \delta \sin \pi x / L \quad (8-19)$$

The maximum bending moment takes place at the center of the span:

$$M_{\max} = EI (\pi / L)^2 \delta \quad (8-20)$$

Then, the maximum stress of the inner cladding generated by the bending moment is:

$$\sigma_{\max} = M_{\max} r_i / I \quad (8-21)$$

$$\begin{aligned} &= E (\pi / L)^2 \delta r_i = 9.9 \times 10^{10} (\pi / 3.66)^2 \times 1.24 \times 10^{-4} \times 3.365 \times 10^{-3} \\ &= 3.05 \times 10^4 \text{ Pa} \end{aligned}$$

where r_i is the internal radius of inner cladding.

Using the same procedure for the 13x13 fuel design yields very similar results with the maximum stress of the inner cladding generated by the bending moment of 39.1 kPa.

Therefore, even though the buckling instability is possible at the inner cladding, the maximum stress in the inner cladding generated by the bending moment is very much limited by the fuel and the outer cladding and many orders of magnitude smaller than yield stress. In conclusion, the buckling instability may not be a real issue for the design of the annular fuel.

8.3 Vortex-Shedding Issues

8.3.1 Basic Theory

As the tube bundle is subjected to cross flow, the vortices will shed alternately from one side and then the other side of the cylindrical tubes. The frequency of the vortex-shedding, f_s , has been found proportional to the cross flow velocity, V , and inversely proportional to the diameter of the tube D . Thus,

$$f_s = SV / D \quad (8-22)$$

where S is the proportionality constant, and is defined by [Weaver and Fitzpatrick, 1988]

$$\text{In-line square array: } S = \frac{1}{2(P/D - 1)} \quad (8-23)$$

The above equation is based on the maximum approach velocity defined by (see Figure 8.2):

$$V_{\text{approach}} = \frac{P - D}{P} V_p \quad (8-24)$$

where

P : pitch of tube bundle

D : outer diameter of tube

V_p : velocity within the narrowest gap distance

V_{approach} : velocity at the subchannel frontal area

Figure 8.2: Relationship of V_p and V_{approach}

As the vortices shed from the tube, a fluctuating force will be generated. It has two components. The lift direction (perpendicular to the flow direction) has a frequency equal to the vortex-shedding frequency f_s . The drag direction (in the direction of flow) has a frequency equal to twice the vortex-shedding frequency $2f_s$.

If the fluctuating force frequency is close to one of the tube natural frequency modes, a phenomenon called lock-in will occur, which can take place both in the lift direction and in the drag direction, resulting in a much larger amplitude of resonance vibration of the tube.

The most important rule in designing components against vortex-induced vibration damage is to avoid the lock-in condition. Although there are several alternate rules, the most common one is the separation rule. If the structural modal frequency is at least 30% below or 30% above the vortex-shedding frequency, i.e., if

$$f_n < 0.7f_s, \text{ or } f_n > 1.3f_s \quad (8-25)$$

then lock-in in the lift direction is avoided in the n th mode. Likewise, if

$$f_n < 0.7 \times 2f_s, \text{ or } f_n > 1.3 \times 2f_s \quad (8-26)$$

the lock-in in the drag direction is avoided in the n th mode.

When the vortex-shedding frequency is well separated from the natural frequency of tubes, based, for example, on the 30% rule, the magnitudes of forced tube vibration are usually small and are bounded by turbulence-induced vibration. The main concern of vortex-shedding in a tube bundle is its potential to excite the acoustic modes in the flow cavity.

8.3.2 Estimation of Critical Cross Flow Velocity to Avoid Vortex-shedding Lock-in

In the annular fuel rod bundles, based on the 30% rule, the criterion to avoid vortex-shedding lock-in is:

$$f_1 > 1.3 \times 2f_s = 1.3 \times 2 \times (SV_{cross} / D) \quad \text{or} \quad V_{cross} < f_1 D / (2.6 \times S) \quad (8-27)$$

From Eq. (8-14), one can see that the first-mode natural frequency of the annular fuel pin is less than in other modes. Thus, if f_1 satisfies Eq. (8-27), the other modes will also be satisfied.

Table 8.6 gives the values of the critical pitch cross velocity in annular fuel bundles using Eq. (8-27). It can be observed that the critical cross velocity is high enough to avoid vortex-shedding lock-in, both in the open gap cases and the closed gap cases. The maximum cross flow velocities calculated by VIPRE-01 at 100% flow were 0.02 and 0.05m/s for the annular and solid fuel, respectively, which are significantly less than the values shown in Table 8.6. Also, it is to be noted that annular fuel is more resistant to lock-in than the solid fuel of the reference case.

Table 8.6: Critical cross flow velocity to avoid vortex-shedding lock-in

Fuel type	Outer diameter D (mm)	Proportional ity constant S	Frequency f_l (Hz)		Critical pitch cross flow velocity(m/s)	
			Open gap	Closed gap	Open gap	Closed gap
15x15	13.245	6.22	64.00	64.5	0.71	0.72
13x13	15.367	7.14	71.59	75.6	0.91	0.96
17x17(ref.)	9.522	1.53	52.38	42.4	0.51	0.41

8.3.3. Acoustic Resonance Issue due to Vortex Shedding

Theoretically, once the vortex-shedding frequency is above the lowest acoustic mode in the tube bundles, the acoustic resonance is very easily excited. The acoustic resonance normally causes intense acoustic noise, which can lead to severe structural damage. However, operating experience shows that even when the theoretical vortex-shedding frequency coincides with one of the acoustic modal frequencies, very often there is no noise problem, because of the lack of organized vortex shedding in the tube bundles. Thus, more realistic guidelines to avoid tube bundles acoustic resonance are needed.

Ziada et. al. [1989] proposed a resonance map to classify the susceptibility of a tube bundle to acoustic resonance based on a resonance parameter, G_i , defined as:

$$G_i = XR_c^{1/2} / R_a \quad (8-28)$$

where

$$X = P / D$$

$$R_c = V_p D / \nu$$

$$R_a = cD / \nu$$

$$c = c_o / (1 + \sigma)^{1/2}$$

ν : kinematic viscosity of the coolant

c_o : the sound speed without any tube

σ : solidity ratio, which is that of the cross section area occupied by the tubes to the total cross section area

$$\text{Thus, } G_i = (P / D)[V_p \nu / (Dc^2)]^{1/2} \quad (8-29)$$

If G_i and X decrease, the system will be less susceptible to a resonance problem. Let us consider the case in which only geometric parameters are different between the annular

fuel assembly and the 17x17 reference fuel assembly. Table 8.7 shows the ratio of the resonance parameter in the annular fuel to that in the solid fuel.

Table 8.7: Ratio of resonance parameter of annular fuel to solid fuel

Fuel type	P/D	D(cm)	Solidity ratio	G_i
15x15	1.08	1.3245	0.673	
13x13	1.07	1.5367	0.466	
17x17	1.3264	0.9522	0.446	
Ratio (15x15/17x17)	0.814	1.39	1.51	0.57
Ratio (13x13/17x17)	0.809	1.54	1.044	0.64

As expected, G_i and X in both the 15x15 and 13x13 annular fuels are much smaller than those in the 17x17 reference assembly. Therefore, the annular fuel assemblies are more likely to avoid the resonance of the acoustic pressure wave than the 17x17 fuel assembly. As the 17x17 fuels are not susceptible to this vibration mode, the annular fuel can be expected to be even more resilient.

8.4 Acoustic Resonances of the Inner Channel Due to Pump Blades Pass

In addition to the acoustic resonance due to vortex shedding in the tube bundles, acoustic resonance of the inner channel with pump-blade passing frequencies needs to be addressed. If the geometry is the only difference of the new 15x15 and 13x13 lattices, they will have the same acoustic property. The natural acoustic frequency is obtained as

$$f_a = c / 2\pi \sqrt{(\lambda / R)^2 + (\alpha\pi / L)^2} \quad (8-30)$$

where $\lambda = 0, 1.8412, 3.0542, 3.8317$

As other modes of λ except for $\lambda=0$ mode produce very high acoustic frequencies (higher than 10^4Hz), one can safely neglect these terms to obtain the lowest acoustic frequency with a mode of $\lambda=0$:

$$f_{a\alpha} \approx (\alpha c / 2L) \quad (8-31)$$

$$f_{a1} = c / (2L), f_{a2} = 2f_{a1}, a = 1, 2, 3, \dots$$

Au-Yang [2001] gives the blade passing frequencies for reactor coolant pumps (RCP) as 100HZ, 200HZ, 300HZ...etc. The sonic velocity ranges from 1012.8 to 806 m/s for the inlet and outlet temperature range between 292 and 325°C, respectively, yielding frequencies of 132.9 and 105.8Hz. Assuming the average sonic velocity gives the average acoustic frequency of 119.4 m/s. Table 8.8 compares the natural acoustic and pulsation frequencies.

Table 8.8: Comparison between the acoustic frequency and pulsation frequency from a RCP

	First mode	Second mode	Third mode
Acoustic frequency (Hz)	119.4	238.8	358.2
Pulsation frequency (Hz)	100	200	300

Table 8.8 shows the acoustic frequencies to be within 120% of the pulsation frequency. When the structure natural frequency coincides with the surrounding fluid natural frequency in the same vibration mode, a phenomenon designated as complete coincidence occurs, which may lead to components fatigue failure. Eisinger [1970] recommended that the frequency separation should be above 25 percent (only if accurate predictions are available, this separation can be reduced). It is seen that the inner channel of the annular fuel does not satisfy this criterion. Further work is needed to see how the acoustic resonance of the inner channel cladding with pump-blade passing frequencies affects the fatigue failure of the annular fuel.

8.5 Fluid-Elastic Instability

When a tube bundle is subjected to a cross flow, the tubes may vibrate with an amplitude that will increase as the cross flow velocity increases. At some high cross velocity, the vibration amplitude of the tubes is suddenly increased, until tube impacting occurs. This phenomenon is called fluid elastic instability. The velocity at which the vibration amplitude of the tubes suddenly increases is called the critical velocity. Unlike vortex shedding, the vibration amplitude of the unstable tube bundle will continue to increase as the cross flow velocity increases.

In 1970, Connors derived an empirical equation for predicting the critical velocity based on simple dimensional analysis. His correlation is:

$$V_c = \beta f_n [2\pi \zeta_n M_t / \rho]^{1/2} \quad (8-32)$$

where

β : Connors' constant

ζ_n : damping ratio

ρ : fluid density around the tube

Because of its simplicity, Connor's equation [Connors, 1970] has been widely accepted by the industry as the standard way of assessing fluid elastic instability. It was the only recommended equation for predicting the critical velocity in a tube bundle in the 1998 version of the ASME Boiler Code.

8.5.1 Discussion of Connor's Equation

1) Connor's constant β :

Pettigrew [1998] found that there is a P/D effect on the value of Connors' constant, β . The effect of P/D in single-phase flow is very small, but in two-phase flow it is very large. It is thought that hydrodynamic coupling between tubes, which is required for fluid-elastic instability, is much enhanced by confinement in two-phase flow.

2) Damping ratio ζ

The damping ratio for a loosely supported tube is known to be higher than that for a tightly supported one as shown in Table 8.1. Also, the damping ratio in two-phase flow is higher than in single-phase water flow, because the chaotic bubble behavior enhances energy dissipation.

3) Fluid-elastic stability margin (FSM)

Fluid-elastic stability margin is defined as:

$$FSM = V_c / V_p \quad (8-33)$$

If $FSM > 1.0$, the tube will be stable.

8.5.2 Estimation of Critical Cross Flow Velocity to Avoid Fluid-elastic Instability

According to Lu et. al. [2001], no fluid-elastic instability has been encountered even at a maximum cross velocity of 1.6m/s even for gapped mid-grids in the VIPER(Vibration Investigation and Pressure-drop Experiment Research) loop, which is a Westinghouse full-scale fuel assembly test facility. Therefore, Connors' correlation produces very conservative results.

Table 8.9: Critical cross flow velocity to avoid fluid-elastic instability

Open gap case	Fuel type	β	f_{lcl0} (Hz)	damping ratio	M_t (Kg/m)	Density(Kg/m ³)	V_c (m/s)
	15x15	2.4	64.00	0.01	0.41	740.5	0.906
	13x13	2.4	71.5	0.01	0.52	740.5	1.14
	17x17(ref.)	2.4	52.38	0.01	0.216	740.5	0.538
Closed gap case	Fuel type	β	f_l (Hz)	damping ratio	M_t (Kg/m)	Density(Kg/m ³)	V_c (m/s)
	15x15	2.4	64.5	0.01	1.20	740.5	1.56
	13x13	2.4	75.6	0.01	1.53	740.5	2.07
	17x17(ref.)	2.4	42.4	0.01	0.774	740.5	0.82

Using Connors' constant with the conservative damping ratio and the natural frequency of the annular fuel for both the open and closed gas gap cases, the critical cross velocity to avoid fluid-elastic instability can be calculated, as shown in Table 8.9. It is seen that a higher critical velocity exists in the annular fuel case than in the reference conventional fuel case. With respect to critical cross-flow velocity to avoid fluid-elastic instability, the annular fuel performance is superior to that of solid reference fuel.

8.5.3 Nondimensional Axial Velocity to Avoid Fluid-elastic Instability

According to Paidoussis [1982], the fluid-elastic instability due to axial flow in the fuel bundle can be avoided if the following condition is satisfied:

$$u^* = VL\sqrt{\rho A/(EI)} < 1.5 \quad (8-34)$$

In both the traditional and proposed annular PWR fuel assemblies, the nondimensional axial velocity is much smaller than 1.5 as shown in Table 8.10.

Table 8.10: Nondimensional axial velocity to avoid fluid-elastic instability

Open gap	Fuel type	V(m/s) (100% flow)	L(m)	M _i (Kg/m)	I _{clo} (Kg.m ²)	E _{cl} (MPa)	u*(100% flow)	u*(150% flow)
	15x15	4.2	0.508	0.102	4.58E-10	9.91E+10	0.97E-01	1.46E-01
	13x13	4.2	0.508	0.137	7.28E-10	9.91E+10	0.93E-01	1.39E-01
	17x17(ref.)	5.0	0.508	0.0527	1.62E-10	9.91E+10	1.31E-01	N/A
Closed gap	Fuel type	V(m/s) (100% flow)	L(m)	M _i (Kg/m)	I(Kg.m ²)	E _i (MPa)	u*(100% flow)	u*(150% flow)
	15x15	4.2	0.508	0.102	1.34E-9	1.0E+11	5.92E-02	8.84E-02
	13x13	4.2	0.508	0.137	2.36E-9	1.0E+11	5.17E-02	7.76E-02
	17x17(ref.)	5.0	0.508	0.0527	3.76E-10	1.0E+11	9.55E-02	N/A

8.6 Turbulence Induced Vibration

In parallel and cross flows a structure is subjected to turbulent buffeting due to the random pressure perturbations of the fluid stream acting on the cylindrical surface. The structure acts like a band-absorption filter, extracting energy from the turbulent field in narrow windows about its natural frequency. In this work, the first mode vibration amplitudes both in axial flow and cross flow are investigated, respectively. Then, the combination of these two vibrations is estimated.

8.6.1 Axial Flow Induced Vibration

Let us consider a sub-set of axial flow-induced vibration problems in which the fuel is basically one-dimensional with the parallel flow along its axis. For practical application Au-Yang [2001] recommends Paidoussis' equation, which estimates the upper bound response. Here, the upper bound means 99.7% of the time the vibration amplitude at the

mid-span of the tube is less than $3*y_{rms}$, assuming the response follows a Gaussian distribution.

$$y_{max} / D = (5E - 4)K\alpha^{-4} [u^{1.6} \varepsilon^{1.8} \text{Re}^{0.25} / (1 + u^2)] [D_h / D]^{0.4} [\beta^{2/3} / (1 + 4\beta)] \quad (8-35)$$

where

$$u = VL[(\rho A)_{fo} / (EI)]^{1/2},$$

L: fuel length between the grids,

V: axial fluid velocity in the outer channel,

E: Young's module of cladding, and

I: area moment of inertia.

$\alpha = \pi$ for simply-supported rods,

K=5 for turbulent flow,

$$\varepsilon = L / D,$$

$$\text{Re} = VD/\nu,$$

$$D_h/D = \text{hydraulic diameter/tube diameter} = [(4/\pi)(P/D)^2 - 1],$$

P/D=pitch/diameter,

$$\beta = (\rho A)_{fo} / [\rho_f (A_{fi} + A_{fo}) + (\rho A)_{fuel}],$$

$$(\rho A)_{fuel} = (\rho A)_{cli} + (\rho A)_{clo} + (\rho A)_f,$$

Subscripts fo and fi: fluid in the outer and inner channels

Using 100% axial flow velocity for the annular fuel of 4.2 m/s both in the 15×15 and 13×13 cases, and 5.0 m/s for the solid fuel yields the results shown in Table 8.11. Also shown are the cases of 150% flow.

Table 8.11: Maximum amplitude for axial flow-induced vibrations (meter)

	Open gap 100% flow	Closed gap 100% flow	Open gap 150% flow	Closed gap 150% flow
y_{max} -15×15	8.62E-06	3.66E-06	1.81E-5	7.74E-6
y_{max} -13×13	6.48E-06	2.63E-06	1.37E-5	5.56E-06
y_{max} -17×17 Ref.	6.97E-05	3.60E-05	N/A	N/A
Ratio(15x15/Ref.)	0.124	0.102	0.26	0.215
Ratio(13x13/ Ref.)	0.093	0.073	0.196	0.154

The annular fuel has a smaller clearance between fuel rods (see Figure 8-2) than the solid fuel. A comparison of y_{max} /clearance between the annular and solid fuels is given in Table 8.12.

Fig. 8.2 Definition of fuel rods clearance

Table 8.12: Maximum amplitude to gap clearance ratios for axial flow-induced vibrations

	g (mm)	Open gap 100% flow ($y_{\max}/g$)	Closed gap 100% flow ($y_{\max}/g$)	Open gap 150 % flow ($y_{\max}/g$)	Closed gap 150%flow ($y_{\max}/g$)
15×15	1.065	8.1E-03	3.44E-03	1.7E-02	7.27E-03
13×13	1.143	5.67E-03	2.30E-03	1.19E-02	4.86E-03
17×17 Ref.	3.108	2.24E-02	1.16E-02	N/A	N/A
Ratio (15×15/ Ref.)		0.361	0.297	0.76	0.628
Ratio (13×13/ Ref.)		0.253	0.199	0.533	0.420

8.6.2 Cross Flow Induced Vibrations

If the modal frequencies are well separated, Au-Yang [2001] recommended the following correlation to assess the root mean square response to turbulence cross flow-induced vibration in the mid plane of a rod span:

$$\langle y^2 \rangle = \sum_n \frac{LG_F(f_n)\phi_n^2(x)}{64\pi^3 M_n^2 f_n^3 \zeta_n} J_{nm} \quad (8-36)$$

The above correlation suggests that $\langle y^2 \rangle \propto 1/f_n^3$. Since the frequencies are well separated both in the annular fuel and the solid fuel, only the first vibration modal is considered. Thus,

$$\langle y^2 \rangle = \frac{LG_F(f_1)\phi_1^2(x)}{64\pi^3 M^2 f_1^3 \zeta_1} J_{11} \quad (8-37)$$

where

L: axial fuel rod span between two grid spacers

G_F : random force PSD

f_1 : first modal frequency

$\phi_1(x)$: first modal shape function. Its maximum value is the $\phi_{\max} = \sqrt{\frac{2}{L}}$

M: total linear mass

ζ : damping ratio, equals to 0.01 for loosely supported tubes

J_{11} : joint acceptance integral, assuming $\lambda/L=1.0$, where λ is correlation length.

This value was set to 0.64 following Appendix 9B of Au-Yang [2001].

Since the maximum cross flow velocities calculated by VIPRE-01 at 100% flow were 0.02 and 0.05m/s for the annular and solid fuel, respectively, to be conservative, 0.05 m/s for the annular fuel, and 0.1 m/s for the solid fuel were assumed in the calculations to

obtain the results summarized in Table 8.13. Table 8.14 lists the y_{rms}/g ratios and relative comparison of these ratios between the annular fuel and the solid fuel.

Table 8.13: Vibration amplitude for cross-flow induced vibrations (meter)

	Open gap 100% flow	Closed gap 100% flow	Open gap 150% flow	Closed gap 150% flow
15×15	1.54E-07	5.11E-08	3.46E-07	1.15E-07
13×13	8.98E-08	2.44E-08	2.02E-07	5.48E-08
17×17 Ref.	1.73E-06	9.58E-07	N/A	N/A
Ratio(15×15/ Ref.)	0.089	0.053	0.199	0.120
Ratio(13×13/ Ref.)	0.052	0.025	0.117	0.057

Table 8.14: Maximum amplitude to gap clearance ratios for cross flow-induced vibrations

	g (mm)	Open gap 100% flow (y_{rms}/g)	Closed gap 100% flow (y_{rms}/g)	Open gap 150% flow (y_{rms}/g)	Closed gap 150% flow (y_{rms}/g)
15×15	1.065	1.44E-04	4.8E-05	3.25E-04	1.08E-04
13×13	1.143	7.86E-05	2.13E-05	1.77E-04	4.8E-05
17×17 Ref.	3.108	5.58E-04	3.08E-04	N/A	N/A
Ratio(15×15/ Ref.)	N/A	0.259	0.156	0.582	0.350
Ratio(13×13/ Ref.)	N/A	0.141	0.069	0.317	0.156

8.6.3 Combination of Axial Flow and Cross Flow

According to Rigaudeau and Morel [2001], for combining the responses to axial flow with cross flow, the arithmetic sum of the contributions for each mode, i , can be used:

$$y_i)_{rms} = y_i)_{rms,axial} + y_i)_{rms,cross}, \quad (8-38)$$

Since the fundamental mode has the largest impact on vibration, and the modes are well separated, only the first mode will be calculated.

We shall use the root mean square response to cross flow obtained from the above calculation. To obtain the root mean square response of the mid-plane to axial flow, three approaches described by Au-yang [2001] can be used. All of them are consistent with each other. Since the upper bound response was obtained in the above computation, the zero-peak amplitude method was employed in the present evaluations. Assuming the response follows a Gaussian distribution yields:

$$y_{rms-axial} = y_{max}/3. \quad (8-39)$$

Table 8.15 summarizes the results and Table 8.16 lists the ratios of the root mean square amplitude to the gap clearance.

Table 8.15: Root mean square amplitude for combination of axial and cross-flow (meter)

	Open gap 100% flow	Closed gap 100% flow	Open gap 150% flow	Closed gap 150% flow
15×15	3.03E-06	1.27E-06	6.38E-06	2.70E-06
13×13	2.25E-06	9.01E-07	4.77E-06	1.91E-06
17×17 Ref.	2.50E-05	1.30E-05	N/A	N/A
Ratio(15x15/ Ref.)	0.121	0.0981	0.256	0.208
Ratio(13x13/ Ref.)	0.0901	0.0695	0.191	0.147

Table 8.16: Root mean square amplitude to gap clearance ratio for combination of axial and cross-flow

	g (mm)	Open gap 100% flow (y _{rms} /g)	Closed gap 100% flow (y _{rms} /g)	Open gap 150% flow (y _{rms} /g)	Closed gap 150% flow (y _{rms} /g)
15×15	1.065	2.84E-03	1.19E-03	5.99E-03	2.53E-03
13×13	1.143	1.97E-03	7.88E-04	4.17E-03	1.67E-03
17×17 Ref.	3.108	8.03E-03	4.17E-03		
Ratio(15x15/ Ref.)	N/A	0.354	0.286	0.746	0.607
Ratio(13x13/ Ref.)	N/A	0.245	0.189	0.519	0.400

From the above tables, it is seen that the annular fuel has smaller turbulence responses than the solid fuel both in axial flow and cross flow, as well as for their combination, even with a 50% flow velocity increase. Moreover, the 13×13 annular fuel array has a better performance than the 15×15 array. This performance of the annular fuel is the result of the higher rigidity of the annular fuel rod compared to that of the solid rod.

8.7 Wear Analysis in the Annular Fuel Assembly

There are basically three types of wear mechanisms caused by flow-induced vibration - impact wear, sliding wear, and fretting wear. These three wear mechanisms are investigated in this section, and a comparison between the annular fuel and the solid fuel is made.

8.7.1 Impact Wear

The gap size between the fuel and the spacer grid highly affects damping, vibration amplitudes, and wear. Initially, the gap is closed to provide tight support for the fuels but

later the vibration and wear result in loosening the support and opening of the gap, which is usually found at the end of life under reactor conditions [Lu et al., 2001].

Surface stress due to impact between the tubes and loose support plates is estimated using the following relation [Au-Yang, 2001]:

$$\sigma_{rms} = c[E^4 M_e f_n^2 \langle y^2 \rangle_{max} / D^3]^{1/5} \quad (8-40)$$

where

c : contact stress parameter, which is a function of the ratio of diametral clearance to D , given in Figure 11.13 of [Au-Yang, 2001]

$\langle y^2 \rangle_{max}$: maximum mean square vibration amplitude of the tube in the adjacent spans

D : outer diameter of tube

M_e : effective mass of tube, equals 2/3 of total mass of two spans of the tube

f_n : natural frequency of the tube

When the surface stress is below the endurance limit for the particular material, impact wear is usually not a problem. If the surface stress is above the endurance limit, then surface damage due to impact wear may occur. Thus, the condition $\sigma_{rms} < \sigma_{endurance_limit}$ needs to be satisfied.

Let us assume that the annular fuel and the solid fuel have the same ratio of grid diametral clearance to fuel outer diameter D . Then, the contact stress parameters are equal in the annular fuel and the solid fuel according to Au-Yang [2001]. Thus, the following equation can be used to rate annular fuel against solid fuel, yielding the results in Table 8.17:

$$\frac{\sigma_{annular}}{\sigma_{solid}} = \left(\frac{M_{t-annular} * f_{1-annular}^2 * y_{rms-annular}^2 * D_{solid}^3}{M_{t-solid} * f_{1-solid}^2 * y_{rms-solid}^2 * D_{annular}^3} \right)^{1/5} \quad (8-41)$$

Table 8.17: Surface stress ratios from impact wear

Surface stress ratio	100% flow velocity	150% flow velocity
Ratio(15×15/17×17)	41.9%	56.6%
Ratio(13×13/17×17)	37.3%	50.4%

It is clear that the annular fuel has a smaller surface stress than the solid fuel. Therefore, the annular fuel should exhibit a better performance in the impact wear problem even at 150% flow rate.

8.7.2. Sliding Wear

The sliding wear takes place between two components that are always in contact with each other. In this case, the volumetric wear, Q , is estimated using the following relationship:

$$Q = KF_n S \quad (8-42)$$

where

K : wear coefficient (1/Pa)

F_n : normal reaction force between two components, N

S : total sliding distance (m) defined as

$$S = \pi f_n g t \quad (8-43)$$

g : gap clearance, assuming it is proportional to tube diameter, m.

t : total elapsed time, in second

$$F_n = \pi D y_{\max} / [0.5(L^2 / A_m E + D^2 L^2 / 4EI)] \quad (8-44)$$

A_m : metal cross-sectional area of the tube

$y_{\max}$: zero-peak vibration amplitude, equals to $3 y_{rms}$

L : length of the span of the tube between two tube-support plates

The wear coefficient, K , is obtained from experimental measurements. It is a function of the material pair and the ambient conditions under which the wear occurs, as well as the wear mode (impact, sliding or fretting). Because of the lack of experimental data, the same K was used for the annular fuel as for the solid fuel. This is a fairly good assumption since both the cladding and grid material are the same and fluid pressure and temperature conditions are very close.

The volumetric wear ratio between the annular and the solid fuels is estimated using the following ratio:

$$\frac{Q_{annular}}{Q_{solid}} = \frac{(D_{annular}^2 * f_{annular} * y_{rms-annular})}{(D_{solid}^2 * f_{solid} * y_{rms-solid})} \cdot \frac{(L^2 / A_m E + D_{solid}^2 L^2 / 4EI)}{(L^2 / A_m E + D_{annular}^2 L^2 / 4EI)} \quad (8-45)$$

Assuming uniform sliding wear, one can define the wear depth as

$$\text{Dept} = Q / (\text{wear surface area}) = \frac{Q}{\pi D a} \quad (8-46)$$

where

Q : volumetric wear,

D : diameter of fuel rod, and

a : spacer thickness in the vertical direction.

Table 8.18: Sliding wear volumetric wear and depth ratios

	Open gap 100% flow velocity	Open gap 150% flow velocity	Closed gap 100% flow	Closed gap 150% flow
Volumetric wear ratio(15×15/17×17)	41.4%	87.3%	50.3%	107%
Volumetric wear ratio(13×13/17×17)	34.7%	73.7%	72.7%	154%
Wear depth ratio(15×15/17×17)	29.8%	62.7%	36.1%	76.8%
Wear depth ratio(13×13/17×17)	21.5%	45.6%	45.1%	95.5%

Table 8.18 indicates that, after the fuel rod gap closes, the annular fuel will have a larger sliding volumetric wear than the solid fuel for 150% flow velocity, but the larger fuel rod diameter of the annular fuel is beneficial in reducing the wear depth to slightly below that of the solid fuel.

8.7.3 Fretting Wear

The fretting wear is caused by a combination of impact and sliding of metals with very small vibration amplitudes, such as turbulence-induced vibration. The volumetric fretting-wear damage rate is estimated using the following Archard's wear equation [Au-Yang, 2001]:

$$\dot{Q} = K\dot{W} \quad (8-47)$$

where

$\dot{Q}$: volumetric fretting-wear damage rate

$\dot{W}$: wear work rate, $\dot{W} = \int F_n dS / \Delta t = 32\pi^2 m L f^3 y_{rms}^2 \zeta$

F_n : normal component of the contact force

S : sliding distance, $S = 2\pi D y_{\max} f_n t / L$

K : wear coefficient

m : linear mass

L : span length

y_{rms} : mid-span RMS vibration amplitude

f : natural frequency

Again, assuming that the annular and the solid fuels have the same wear coefficient, “ K ”, one has the following ratio of the volumetric wear rate in the annular fuel to that in the solid fuel:

$$\dot{Q}_{annular} / \dot{Q}_{solid} = \dot{W}_{annular} / \dot{W}_{solid} = \frac{m_{annular} \cdot f_{annular}^3 \cdot y_{rms-annular}^2}{m_{solid} \cdot f_{solid}^3 \cdot y_{rms-solid}^2} \quad (8-48)$$

Equation (8-48) yields the results summarized in Table 8.19. Clearly, the annular fuel has significantly smaller fretting wear, even at 150% flow rate.

Table 8.19: Volumetric wear rate ratios for fretting wear

	Open gap 100% flow	Closed gap 100% flow	Open gap 150% flow	Closed gap 150% flow
Volumetric Wear rate ratio (15x15/17x17)	5.09%	5.24%	22.6%	23.6%
Volumetric Wear rate ratio (13x13/17x17)	5.00%	5.41%	22.5%	24.3%

8.8 Summary and Conclusions

Comparative analyses of annular fuel rods show better mechanical behavior than the reference solid fuel in a 17x17 lattice with respect to the vortex-induced vibration, fluid-elastic instability, and turbulent-induced vibrations, even at 150% flow rate. Also, fretting, sliding and impact wear are smaller in annular fuel. This is primarily due to the larger inertia of the tube of larger dimensions versus that of the thinner solid rod. Moreover, the 13x13 design has higher margins than the 15x15 fuel, also due to its higher rigidity. Regarding vortex-induced vibrations, a smaller cross flow velocity also contributes to the resistance of annular fuel to this phenomenon. Hence, from the vibration viewpoint, the 13x13 annular fuel design is more resistant to vibration modes and such issues do not pose a limit to core flow rate increases of up to 50%.

Buckling instability may be an issue for the inner cladding because of the very long span and lack of grid support. However, the fuel and the outer cladding would limit the maximum stress in the inner cladding generated by the bending moment, so buckling should not pose a problem.

Analysis of acoustic resonance of the inner channel cladding with pump blade passing frequencies showed that the acoustic frequencies are within 120% of pulsation frequency. Further work is needed to see how the acoustic resonance of the inner channel cladding with pump-blade passing frequencies affects the fatigue failure of the annular fuel.

9. REACTOR PHYSICS ANALYSIS

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In this chapter, we evaluate reactor the physics performance of typical PWR cores fueled with the annular fuel and compare them to the reference solid fuel. First, lattice-level evaluations are conducted using MCNP-4C2 [Briesmeister et al., 2000]. Several unique characteristics of the annular fuel are identified, e.g., double layers of plutonium rim structure for the annular fuel. At the same time, the deterministic lattice code, CASMO-4 [Edenius et al., 1995], is benchmarked and calibrated. Then, a quarter PWR core is modeled using the state-of-the-art Studsvik Scandpower core modeling package including CASMO-4, TABLES-3 [Verplanck, 1995], and SIMULATE-3 [Digiovine, 1995]. The reference core design is chosen to be an equilibrium Westinghouse 4-loop 18-month-cycle PWR model with a rated thermal power of 3411 MWt. Two elaborate annular fuel core designs, 100%-power and 150%-power, are explored, both of which assume the same number of reload fuel assemblies and an 18-month cycle at a 90% capacity factor. The steady-state core performance parameters, such as power peaking limits and critical boron concentrations, are subsequently compared to that of the reference core.

9.1 Overview of Analysis Tools & Lattice Benchmarks

9.1.1. Overview

Reactor physics analysis methods are largely mature nowadays, such that the plant operations of existing LWR cores can be well predicted. The core management system (CMS) code package (CASMO-4, TABLES-3, and SIMULATE-3) developed by Studsvik Scandpower Inc. is one example. It follows a deterministic, multi-group approach so that steady-state core calculations with feedback effects have moderate computational requirements. However, the annular fuel design changes the basic rod configuration, which might lead to inaccurate results when using existing deterministic codes. The Monte Carlo based method is an ideal tool to benchmark results from deterministic codes. The code, MCNP-4C [Briesmeister, 2000], developed by Los Alamos National Laboratory (LANL) was used together with a one-group point-depletion code, ORIGEN-2.2 [Croff, 1980] via a burnup linkage program, MCODE-1.0 [Xu et al., 2002a]. Although more rigorous and generic compared to deterministic codes, the Monte Carlo runs are computationally more demanding, e.g., even a typical pin burnup calculation requires hours of running on a modern GHz-CPU computer. A detailed core burnup calculation using the Monte Carlo method is still not realistic at this time. Hence, the major purpose of Monte Carlo calculations is to provide a reference solution in order to calibrate results from deterministic codes. Note that since the source code of the CMS code package is not available to MIT, only the input can be artificially adjusted in several ways to match key MCODE results.

9.1.2. Overview of Computer Codes

The Studsvik core management system (CMS) includes CASMO-4, TABLES-3, and SIMULATE-3. Combined together, these licensing-level computer codes are capable of simulating steady-state LWR core operations accurately and are widely used by nuclear utilities to do routine reactor core reload calculations, as well as by fuel vendors. In general, users run the code package in a tandem way, i.e., many CASMO-4 depletion cases are first prepared under different core conditions for each type of fuel assembly, TABLES-3 then reads CASMO-4's output card image files (containing two-group cross sections and other parameters) and tabulates them into a binary-format library; then SIMULATE-3 core calculations can be performed using the generated library.

The lattice physics code, CASMO-4, is a multi-group two-dimensional transport theory code entirely written in Fortran 77. It is used for burnup calculations of LWR assemblies or pin cells, i.e., geometries consisting of cylindrical fuel rods of varying compositions in a square/hexagonal lattice. When modeling the annular fuel, the accuracy of CASMO-4 is a primary concern due to the significant deviation from the traditional fuel pin geometry. Therefore, MCNP/ORIGEN depletion calculations are used later in this section to benchmark results from CASMO-4. Note that, in the CMS code suite, CASMO-4 functions as the few-group cross section generator for SIMULATE-3, i.e., a series of CASMO-4 depletion calculations are needed for a given assembly design in order to generate few-group data in terms of burnup, fuel temperature, moderator temperature, boron concentration, history variables, etc. A realistic few-group preparation procedure was used in this work [Weader, 2000], which produces 3-dimensional data tables for each fuel segment.

SIMULATE-3 is an advanced three-dimensional, two-group nodal code for LWR steady-state core analysis. It performs a coupled neutronics/thermal hydraulics iteration to calculate the detailed core power distribution. The reactor core is represented by dividing it into a small number of sizable regions called nodes. Homogenized parameters for each of these nodes are obtained from the lattice physics code, CASMO-4. The advanced nodal method with usage of discontinuity factors is formulated on the basis of integrating the three-dimensional transport equation over two transverse directions to obtain a one-dimensional diffusion equation. No user adjustments or external tuning are required in SIMULATE-3. Since the core physics of annular fuel is not expected to be significantly different from a conventional core, there is less concern about applying SIMULATE-3 to model the PWR core with annular fuel.

Besides the CMS code package, Monte Carlo based codes are used to benchmark CASMO-4 results, i.e., MCNP-4C2, ORIGEN-2.1, and MCODE-1.0. MCNP-4C2 is a general purpose, generalized-geometry Monte Carlo transport code developed by the Los Alamos National Laboratory. Track length estimators are used to obtain reaction rates for various nuclides in the fuel. MCODE-1.0 is a linkage code that combines MCNP and ORIGEN2 to do depletion calculations. As a matter of fact, this approach to Monte Carlo depletion is an interesting problem. The MCNP calculations of reaction rates have intrinsic stochastic scatter, which are then introduced into the deterministic burnup equations in ORIGEN2 as small random perturbations. However, it turns out that the burnup equation itself is robust and fairly insensitive to these perturbations. In practice,

these stochastic diversions tend to cancel each other as burnup progresses. It should also be noted that it is extremely time consuming to do an accurate MCNP calculation with a few tenths of a percent uncertainty in the pin power distribution for a typical fuel assembly. On the other hand, the eigenvalue calculation with MCNP is fairly efficient. For the annular fuel, the Monte Carlo codes are only used as supplementary codes to provide reference solutions for benchmarks and some lattice physics calculations. While the applicability is not deemed an issue, the CPU requirement makes it unrealistic for routine Monte Carlo production analysis.

9.1.3 Lattice Physics Benchmarks

Using a pin cell model, CASMO-4 was benchmarked against the Monte Carlo burnup code package, MCNP-4C/ORIGEN-2.1/MCODE-1.0, for a typical PWR solid fuel design and two annular fuel designs. All cases are poison-free, i.e., neither burnable poison nor soluble poison is considered. It is assumed that CASMO-4 produces accurate results for a typical PWR fuel designs. The difference between CASMO-4 and MCODE-1.0 for the typical PWR solid fuel is taken as a frame of reference. The quality of agreement between CASMO-4 and MCODE-1.0 for the annular fuel designs is then examined by comparison to this reference difference to evaluate the validity of CASMO-4 results for the annular fuel.

Geometric configurations of unit cell models, taken from a typical PWR assembly and the annular designs, are shown in Figure 9.1. For all three benchmark problems in this section, the fuel is UO_2 at 4.95 w/o enrichment and 95% theoretical density (10.4 g/cm^3), and periodic boundary conditions are imposed on the four edge surfaces of the unit cell. All problems assume hot operating conditions at a core power density of $104.5 \text{ kW/liter-core}$. The fuel temperature is 900 K for the solid fuel and 600 K for the annular fuel. The water temperatures for all cases are the same at 583.1 K. The two annular fuel designs considered are labeled as PQN-01 (15×15 lattice) and PQN-02 (13×13 lattice). The overall assembly size is nevertheless the same as the typical Westinghouse PWR 17×17 -lattice assembly. The geometric dimensions are given in Table 9.1. Note that except for H_2O , thermal expansions are ignored in all calculations.

The infinite multiplication factors versus burnup are plotted in Figure 9.2. The eigenvalue differences are much larger for the annular fuel designs than the reference solid fuel, as further shown in Figure 9.3. There is a satisfactory agreement between CASMO-4 and MCODE-1.0 for the typical PWR solid fuel design (17×17 lattice), which confirms the assumption that CASMO-4 produces accurate results for typical PWRs with solid fuel rods. Not only is the method itself different (deterministic versus stochastic) but also the neutron libraries differ, both in format and in their source. MCNP uses continuous-energy libraries while CASMO-4 uses 70-group cross section libraries. With respect to the source, two major nuclides, U-235 and U-238, adopt continuous-energy, ENDF-V library evaluations in the MCODE-1.0 calculations while the version of CASMO-4 available at MIT is primarily based on 70-group cross section libraries processed from JEF-2.2 libraries. The eigenvalue difference for the reference solid fuel is almost constant which reflects a constant library difference. In contrast, the differences for the annular fuel designs are much larger at the beginning of irradiation and gradually decrease with

burnup. This phenomenon is contradictory to most benchmark calculations, since the beginning of irradiation involves only few nuclides under clearly-specified conditions

Figure 9.1. Schematics of unit cell models (not to scale).

Table 9.1 Geometric data for benchmark problems (in units of cm)

Cases	Solid fuel (17×17)	PQN-01 (15×15)	PQN-02 (13×13)
Pin pitch	1.26	1.431	1.651
Rod inner radius	—	0.3365	0.4315
Inner clad outer radius	—	0.3935	0.4890
Fuel inner radius	—	0.4000	0.4950
Fuel outer radius	0.4096	0.5990	0.7050
Outer clad inner radius	0.4178	0.6050	0.7110
Rod outer radius	0.4750	0.6600	0.7685

* All dimensions shown are cold dimensions in units of centimeters.

In order to examine the reasons behind the trends, two neutronic quantities, C^* and k_{∞} , shown in Table 9.2 for the beginning of life (BOL) conditions, were calculated. C^* is defined as the ratio of U-238 capture rate to U-235 fission rate. Since the total power is the same for both CASMO-4 and MCNP-4C and the fissions at BOL are dominated by the U-235 fission, the U-235 fission rate is therefore almost identical for both CASMO-4 and MCNP-4C. It is seen that for solid fuel the ratio, C^* , is almost the same indicating a similar U-238 capture rate.

Table 9.2 Neutronic parameters from CASMO-4 and MCNP-4C (at BOL)

	C^*		k_{∞}	
	CASMO-4	MCNP-4C	CASMO-4	MCNP-4C
Solid fuel (17×17)	0.504	0.503	1.3915	1.3867(13)
PQN-01 (15×15)	0.496	0.539	1.3878	1.3556(16)
PQN-02 (13×13)	0.455	0.505	1.4173	1.3759(14)

Figure 9.2 Eigenvalue benchmark results of CASMO-4 against MCODE-1.0.

Figure 9.3 Eigenvalue differences between CASMO-4 and MCODE-1.0.

For the annular fuel, CASMO-4 underestimates the U-238 capture rate, which leads to higher eigenvalues compared to MCNP-4C predictions. One can infer from the CASMO-4 calculation flow chart that the resonance calculation in CASMO-4 is responsible for the underestimation of the U-238 captures. Due to the water presence inside of the annular

fuel, the self-shielding effect is reduced and U-238 resonance captures are effective on both sides of the annular fuel pellet. However, equivalence relations for heterogeneous resonance integrals were formulated only for solid cylindrical rods in the CASMO-4 resonance calculations, i.e., CASMO-4 assumes epithermal U-238 captures to occur only at the outer surface of the annular fuel pellet. Thus, the resonance integrals are inadequate for annular fuel and are responsible for the underestimate of U-238 capture rates observed in Table 9.2. While the equivalence relations for reducing the heterogeneous resonance integrals of annular absorbers to homogeneous resonance integrals are well established in the literature [Segev, 1981], there has been less attention to this issue recently since all existing LWRs employ only solid cylindrical fuel rods (or center-voided annular pellets). For the annular fuel, as burnup increases, CASMO-4 generates less plutonium due to fewer U-238 epithermal captures compared to MCODE-1.0, which results in a trend of gradually decreasing eigenvalue for CASMO-4 relative to MCODE-1.0. This shortcoming of CASMO-4 was also confirmed by discussions with Studsvik personnel [Bahadir, 2001].

In summary, this benchmark set shows that the lattice physics code needs to be improved for modeling annular fuel. Several parallel paths might be pursued in order to resolve this issue:

- (1) switch to other lattice physics code, such as HELIOS;
- (2) modify the CASMO-4 source code enabling an appropriate resonance treatment;
- (3) develop a remedy to overcome this CASMO-4 limitation and make its application for the annular fuel designs possible.

The first option involves the considerable time and effort of acquiring and using a new code. Although the second choice is a logical one, MIT does not have access to the CASMO-4 source code. The last option appeared to be the easiest short-term realistic solution for PWR core calculations, and will be described next.

9.1.4 CASMO-4 Adjustments

9.1.4.1. Criteria for Adjustment

The goal of CASMO-4 adjustment is to modify CASMO-4 input so that its results for the average core condition, typical fuel enrichment and typical burnable poison loading (and also for the poison-free case), match the more rigorous MCNP/ORIGEN2 burnup results. A strategy of CASMO-4 input adjustment is first developed for the pin cell (with or without burnable poison), and then extended to the full assembly. It is also recognized that there exists no single CASMO-4 input adjustment which enables CASMO-4 results to exactly match the MCODE-1.0 results under all circumstances. The strategy is rather limited to the average core conditions and for the typical fuel enrichment. The only other dependence considered is the effect of burnable poisons.

9.1.4.2. Poison-Free Pin Cell Adjustment

The benchmark problem from section 9.1.3 will be the starting point. There are several ways to overcome the problem of CASMO-4 underestimation of epithermal U-238 capture rate:

- A fictitious increase in U-238 number densities to increase epithermal U-238 captures;
- a fictitious reduction in coolant density to slightly reduce moderation;
- an addition of small amount of hafnium to suppress the reactivity.

Regarding the first approach, the effective resonance integral (RI_{eff}) for a heterogeneous fuel/moderator configuration can be empirically correlated as [Bell and Glasstone, 1970]:

$$RI_{\text{eff}} = a + b \sqrt{\frac{S}{M}},$$

where a , b are constants, S is surface area of fuel, and M is the fuel mass. The first modification increases M but keeps the same S . Hence, RI_{eff} decreases, i.e., per U-238 atom, the resonance capture probability actually decreases. However, the U-238 capture rate increases due to larger U-238 atom number density ($\sim$ product of M and RI). Figure 9.4 shows the effect of increasing U-238 number densities by 10%, 15%, 20% and 30%. Note that the eigenvalue difference is plotted as a function of energy production per unit core volume (endurance). It is seen that a +20% increase in U-238 contents gives the best agreement between CASMO-4 and the reference MCODE-1.0 results.

Figure 9.4 CASMO-4 input adjustment for poison-free pin cells by increasing U-238.

The other two approaches of recovering reactivity are not as successful as the increase of U-238 number density. Figure 9.5 compares various CASMO-4 adjustments using all three methods. Note that the CASMO-4 adjustment is optimized for each approach. It is clear that the 20% increase of U-238 number densities is the best approach to CASMO-4 input adjustment, which is therefore chosen as the CASMO-4 input adjustment for the poison-free pin cell. In fact, the improved eigenvalue agreement in the entire burnup range also reflects quite good agreement in plutonium composition with burnup, as shown in Figure 9.6.

Figure 9.5 Comparison of three best approaches to CASMO-4 input adjustments for poison-free annular fuel pin cells.

9.1.4.2. Gadolinium-Poisoned Pin Cell Adjustment

Burnable poison usage in PWRs is a standard practice. Although a thin layer of boron coating on the fuel pellet surface, the so called integrated fuel burnable absorber (IFBA), is a typical Westinghouse burnable poison, an alternative integral burnable absorber — gadolinium oxide (Gd_2O_3) — is chosen in this work because it is easier to manufacture compared to IFBA for the annular fuel. Since gadolinium is a strong absorber, the CASMO-4 input adjustment needs to be revisited for gadolinium pins.

As for the poison-free pin cell, the same approach of U-238 number density adjustment was selected. Figure 9.7 compares CASMO-4 results for two cases involving 20% or 30% higher U-238 number density with the target curve calculated by MCODE-1.0. All calculations were performed for the 13×13 PQN-02 design (as shown in Table 9.1) with 4.95 w/o U-235 enrichment and 5.0 wt% Gd_2O_3 . Since Gd is a strong absorber, the fuel pellet is subdivided into 10 equal-volume concentric burnup zones. Note that only the amount of U-238 was artificially increased whereas the gadolinium and oxygen contents are fixed. It is observed that the increase of U-238 content by 30% yields closer

agreement with the MCODE-1.0 results than the +20% case. In this case, the eigenvalue near BOL is determined by gadolinium (almost independent of U-238 variations). The U-238 addition becomes more effective as gadolinium gradually burned out.

Figure 9.6 Plutonium composition changes with burnup for poison-free annular fuel pin cell.

Figure 9.7 CASMO-4 adjustment for Gd-poisoned pin cell.

The change of plutonium composition with burnup was also compared and showed to give similar results as for the poison-free pin cell. With a 30% increase in U-238, the

gadolinium isotope concentration with burnup agrees well with the MCODE-1.0 results. Therefore, a 30% increase in U-238 concentration is adopted for the poisoned pin cell.

9.1.4.3. Full Fuel Assembly Adjustment

Combining the poison-free and poisoned pin cell CASMO-4 input adjustment results, one can naturally propose a modification scheme of CASMO-4 assembly input in such a way that the amount of U-238 is artificially increased by 20% for poison-free fuel pins and by 30% for fuel rods with gadolinium while all other contents are unchanged. This strategy was examined using the 13×13 PQN-02 assembly design.

The assembly under consideration is a 13×13-lattice PQN-02 design consisting of 9 guide tubes and 160 fuel rods. The fuel enrichment is 4.95 w/o. There are 12 gadolinium poisoned rods (5wt% Gd₂O₃) out of 160 fuel rods. The gadolinium rods are located near the guide tubes to reduce the local power peaking. The pin layout is shown in the upper right corner of Figure 9.8. In addition, Figure 9.8 compares the eigenvalues as a function of burnup for the CASMO4-calculated base case and the case having higher U-238 content against those of MCODE. The increase of U-238 contents is not uniform among all fuel rods, but depends on the existence of poisons. It is observed that this strategy of CASMO-4 input adjustment is useful in terms of greatly improving the eigenvalue agreement with MCODE. The base CASMO-4 assembly calculation, i.e., no adjustment to the input, shows similar behavior as the pin cells.

Figure 9.8 Comparison of eigenvalues for typical assembly calculations.

The change in plutonium composition with burnup was also examined. As in the pin cell analysis, good agreement of plutonium isotope content was achieved. Therefore, the approach of fictitious increase of U-238 number density was verified to produce good

results on an assembly scale. SIMULATE-3 core calculations then rely on the library generated from these adjusted CASMO-4 runs.

9.2 Lattice Physics Evaluations

9.2.1. Neutronic Characteristics of the Annular Fuel

Several differences between the solid fuel rod and the annular fuel are of interest to core designers, as follows:

1. Pin size difference

Keeping the same overall assembly dimensions, the annular fuel necessitates larger cell size (pin pitch) than a solid pin to accommodate the inner channel coolant flow. One must balance the inner and outer flow channels of the annular fuel design in terms of both the heat transfer split and coolant flow split so that the overall thermal hydraulic margins are roughly the same for the two channels. It can be seen from the proposed designs (Table 9.1) that the annular pins in 13x13 lattice (selected reference design for annular fuel) are larger than the reference solid pin in a 17x17 lattice. The larger fuel pin dimension reduces the resonance capture; hence increases the eigenvalue.

2. Geometry difference

The annular fuel pin has fuel surfaces cooled from both sides, which increases the fuel surface-to-volume ratio in comparison with a solid fuel pin. The increased resonance capture reduces eigenvalue.

3. Fuel temperature difference

Due to more efficient cooling of the annular fuel (much smaller thermal resistance), the average fuel temperature is much lower than the reference solid pin. Under normal operating conditions, the average fuel temperature is about 900 K for typical solid pins in PWR lattices while it is only about 600 K for annular fuel rods. This projects to an eigenvalue gain due to a smaller penalty from Doppler absorption.

4. Doubling cladding materials

The annular fuel is cooled from both sides. Consequently, both sides require cladding. The amount of Zircaloy is increased significantly, which increases the parasitic neutron absorption and penalizes the eigenvalue.

5. Hydrogen-to-heavy-metal (H/HM) ratio

It has been a historical trend to increase H/HM (less fuel, more water) to achieve better economics via higher discharge burnup (better fuel utilization) [Xu et al., 2002b] for the same enrichment. Compared to a conventional solid fuel pin, the 13x13 annular lattice has about 10% higher water-to-fuel ratio on a unit cell basis. The eigenvalue is thus increased due to better moderation.

Figure 9.9 Illustration of step-by-step changes from solid fuel to annular fuel.

Table 9.3 Neutronic parameters for step-by-step changes.

Cases	Case 0: Ref. solid pin (17×17)	Case 1: Larger pin (13×13)	Case 2: Clean annular fuel (13×13)	Case 3: Clean annular fuel, lower T_{Fuel}	Case 4: Clean annular fuel double clad	Case 5: Clean annular fuel wetter lattice	Case 6: Annular fuel (13×13)
ε	1.410	1.404	1.423	1.420	1.419	1.381	1.376
η	1.910	1.910	1.910	1.910	1.910	1.910	1.911
f	0.9576	0.9549	0.9599	0.9599	0.9565	0.9563	0.9539
p	0.5417	0.5522	0.5218	0.5281	0.5196	0.5472	0.5502
k_{∞}	1.397	1.414	1.361	1.375	1.347	1.380	1.380
ICR	0.403	0.384	0.442	0.427	0.445	0.420	0.410
C^*	0.496	0.473	0.546	0.527	0.550	0.516	0.505
ρ_{28}	5.68	5.33	6.41	6.14	6.46	5.83	5.69
δ_{25}	0.316	0.311	0.328	0.325	0.328	0.294	0.294
FTC (pcm/K)	-3.60	-3.29	-4.73	—	-4.70	-4.96	-4.48
MTC (pcm/K)	-86.3	-78.7	-82.0	-75.9	-84.6	—	-78.2

^a The fast fission factor, ε , is defined as the ratio of the total rate of production of fission neutrons to the rate of production in the thermal energy range with cutoff energy 0.5 eV, which includes epithermal fission contributions [Henry, 1975].

^b The eta factor η , the thermal utilization factor f , and the resonance-escape probability p are defined in the usual way as in Ref.18.

^c The ICR refers to the initial conversion ratio, i.e., the ratio of U-238 captures to U-235 absorptions.

^d The quantity C^* is the ratio of U-238 captures to U-235 fissions. Therefore, $C^* \geq \text{ICR}$.

^e The quantity ρ_{28} refers to the ratio of epithermal to thermal U-238 captures. Again the energy boundary between thermal and epithermal is defined as 0.5 eV.

^f The quantity δ_{25} is the ratio of epithermal to thermal U-235 fissions.

^g The FTC (Doppler coefficient) and MTC are computed as $\frac{\Delta k}{\Delta T}$ respectively. The fuel temperatures are 600 K and 900 K, and the moderator temperatures are 583.1 K and 600 K. Note that these coefficients are poison-free values.

In reality, these differences are effective simultaneously. However, it is possible to illustrate quantitatively their effects by a virtual step-by-step variation [Xu et al., 2002c], as shown in Figure 9.9. The starting point on the left is the reference solid fuel pin cell taken from a typical Westinghouse 17×17 lattice. The end point on the right is the annular fuel PQN-02 design (13×13 lattice). In between (within the box in Figure 9.9), the five different effects mentioned above are individually activated, i.e., except for the differences specified all other parameters are kept the same. The neutronic parameters are then calculated with MCNP-4C (one million histories for each case; hence the small statistical errors are ignored) and shown in Table 9.3. Note that this calculation is for the fresh fuel (4.95 w/o enrichment UO₂).

The geometry effect is seen as the most important effect, which lowers the eigenvalue by 0.053 (from Case 1 to Case 2). The major reason is the significant increase in resonance absorption of fuel (~6% smaller resonance-escape probability) due to increasing the fuel surface-to-volume ratio. The effects of size difference and H/HM effect are comparable, each one of which increases the eigenvalue by about 0.02. The mechanisms responsible are different. Improved moderation lowers the fast fission factor but significantly increases the resonance-escape probability. The fuel temperature gain is offset by the double cladding's parasitic absorption. In summary, the annular fuel (PQN-02) has a lower eigenvalue compared to the solid fuel. However, the conversion ratio for the annular fuel is higher than for the solid fuel (more U-238 surface resonance absorption); hence the annular fuel has a slightly flatter burnup curve.

9.2.2 Lattice Burnup Calculations

To evaluate the reactivity limited burnup potential of annular fuel, burnup calculations were performed using MCODE-1.0 on the pin cell level and compared to the reference solid fuel. The fuel is 4.95 w/o enriched UO₂ for all cases. The geometry data used is given in Table I. Burnup results are shown in Figures 9.10 and 9.11. From Figure 9.10, the annular fuel design PQN-02 is a better design compared to PQN-01. The PQN-02 reference design achieves the same reactivity-limited burnup as the solid pin, which verifies a larger conversion ratio compared to the solid fuel pin. However, Figure 9.11 shows that the annular fuel design PQN-02 falls short of energy generation compared to the solid fuel. This is attributed to the reduced fuel (heavy metal) loading. The additional layer of cladding reduces the available core fuel volume, which leads to a 10% decrease in heavy metal loading. Therefore, the annular fuel needs higher fuel burnup to support the same energy production compared to the reference solid fuel. This requires slightly higher fuel enrichment or use of other means to improve neutron economy. Note that the calculations here still assume the same power level for both the annular fuel and the reference solid fuel. If the annular fuel operates at higher power level, the burnup requirement will increase even more.

Figure 9.10 Depletion curves for the annular fuel designs and solid fuel pin.

Figure 9.11 Comparison of eigenvalue versus irradiation time curves.

9.2.3 Rim Effects

Due to strong U-238 resonance absorption in the epithermal range, plutonium generation on the UO_2 pellet surface is distinctly higher than in the rest of the pellet (so-called rim effect). The plutonium formation contributes to a very high local burnup in the rim region. Another issue with the rim region is that the fuel temperature there is usually low. This unique feature makes rim modeling a key issue for fuel performance analysis, especially for the high burnups envisioned for this fuel. To provide data for fuel modeling and evaluate the behavior of annular fuel in terms of rim effects, the radial distribution of plutonium and power were calculated as a function of burnup and compared to those of the reference solid pin design. The annular fuel is running at the nominal 100% power level.

The calculations were performed using MCODE-1.0. The peripheral zones of the fuel were divided into fine mesh regions and the fuel was irradiated to burnup of 70 MWd/kg. The radial power density profile at various burnups is shown in Figure 9.12. As expected, the rim effect and associated recrystallization of grains occurs for annular fuel on both sides, contrary to solid fuel where only the outer surface forms a rim region. The outer surface exhibits slightly higher peaking due to the higher population of thermal source neutrons from the outer channel than from the inner channel. The power density peak-to-average ratio is smaller for annular fuel than in the solid pin. Hence, the annular fuel burns more uniformly across the pellet (Figure 9.13).

Figure 9.12 Radial power distributions in annular and solid fuel pellets.

Figure 9.13 Local burnup distribution in the annular and solid pellets.

9.3 Equilibrium PWR Core Designs

9.3.1 PWR Core Modeling

A standard 193-assembly, four-loop Westinghouse pressurized water reactor (3411 MWth) operating on 18-month cycles was chosen as the reference plant. As the annular fuel assembly has the same dimensions as the solid fuel assembly, the fuel assembly management is similar. However, for a higher power density fuel, there are changes of the core operating parameters. For example, the nominal fuel temperature is 900 K for the solid fuel, 600 K for the annular fuel at 100% power level, and 800 K for the annular fuel at 150% power level. Therefore, fuel temperature feedback parameters are different for the annular fuel. On the other hand, the thermal hydraulic conditions, such as, inlet, core-average, and core outlet temperatures are maintained unaltered for the uprated cases because power uprate is achieved by increasing the core flow rate proportionally to core power, as discussed in Chapter 6.

A quarter-core, 3-D SIMULATE-3 model is used with 24 axial nodes and 2×2 radial nodes per assembly. Rotational core symmetry is assumed. CASMO-4 generates libraries for SIMULATE-3, which have different base values for the annular fuel and solid fuel due to temperature differences.

The core modeling is based on an equilibrium 18-month-cycle, 3-batch PWR cores, consisting of 72 fresh fuel assemblies, 72 once-burnt fuel assemblies, and 49 twice-burnt

fuel assemblies. An iterative method is used to approach the equilibrium core. Namely, nine successive full power operation cycles are first carried out using the fixed loading pattern and identical reload fuel assemblies to prepare the proper fuel burnup distributions for the equilibrium core, i.e., the 10th cycle. In particular for the first cycle, once-burnt and twice-burnt fuel assemblies were initialized to approximate burnup values estimated according to linear reactivity theory [Driscoll et al., 1990]. Since there are 9 successive cycles, the finally arrived at equilibrium core is independent of these initialization burnup values. It might appear that the number of preparation cycles is large. However, numerical calculations for the heavily-poisoned, higher-burnup cores demonstrate a relatively slow convergence to the equilibrium core. Once the equilibrium core is achieved, one can then perform detailed core analysis.

Figure 9.14 Sensitivity of core depletions to various U-238 concentration adjustments

Before going into core designs of the annular fuel, the SIMULATE-3 core-wise sensitivity of the CASMO-4 input adjustments is investigated to assess the uncertainties introduced by CASMO-4 input adjustments. For simplicity, a poison-free, annular fuel core (based on a typical 3-batch fuel management) at 3411 MWth with 4.95 w/o enrichment is considered. Several levels of U-238 adjustments (+15%, +20%, and +25% increases of U-238 number densities) were made and calculated by CASMO-4. Figure 9.14 shows the critical boron concentration during the hot full power operation as a function of the effective full power days. It can be observed that the sensitivity of critical boron concentration to the value of U-238 number density adjustments is very small. The values of cycle length for the +15%, +20%, and +25% increase of U-238 contents are 471.1, 467.8, and 463.9 EFPDs respectively. The variation of U-238 contents might alter the cycle length by several days. In addition, the power peaking factors were also compared and found to exhibit reasonable agreement among different U-238 adjustments.

Hence, the CASMO-4 adjustments only mildly affect the SIMULATE-3 results. This small uncertainty is sufficient for the purpose of this scoping study and can be easily eliminated by modifying the resonance integral treatment in future versions of the CASMO-4 code.

9.3.2 Equilibrium Core Performance

Three 18-month-cycle equilibrium core designs are described in this section:

- reference 4-loop Westinghouse PWR core at 3411 MWth with solid fuel (WS);
- PWR core at 3411 MWth with the 13×13-lattice, PQN-02 annular fuel (XF);
- 150%-power PWR core (5116.5 MWth) with the PQN-02 annular fuel (XU).

Note that the core designs are denoted by the symbol in parentheses. Considerable effort was devoted to designing these cores by a trial-and-error approach, i.e., the optimization is performed by human judgment. Three core design targets in this work include: (1) 18-month-cycle with a capacity factor of 90%; (2) peak critical boron concentration ≤ 1750 ppm; and (3) power peaking during the cycle satisfies the hot channel factor, $F_{\Delta h} \leq 1.65$, and the hot spot factor, $F_q \leq 2.5$.

Figures 9.15 through 9.17 show the loading map and the EOC power/burnup distributions of the equilibrium core designs. The generic core design philosophy follows the existing 3-batch core fuel management. The 72 reload assemblies were subdivided into two enrichment levels: 48 and 24 with higher and lower enrichment, respectively. The sub-batch with higher enrichment stays in the core for 3 cycles whereas the lower-enriched fuel remains for 2 cycles. The fuel ID is explained as follows: The first digit (0, 1, or 2) indicates fresh, once-burnt, or twice-burnt fuel assemblies. The letter H and L designate high and low enrichment. The first two numbers after the letter stand for the number of rods with gadolinium, and the last two numbers designate the weight percentage of Gd_2O_3 in these rods (e.g., 80 stands for 8.0 wt% and 10 stands for 10 wt%). Table 9.4 shows the split of enrichment levels and poison loading for these cores. The example of assembly layouts with gadolinium poisoned rods for the 150% annular core (XU) is shown in Figure 9.18. More details on the models and gadolinium pin distribution are given in Xu et al. [2004].

The annular fuel requires an enrichment level beyond 5 w/o to keep the 18-month-cycle and 3-batch fuel management. Note that the fuel residence time in the core for all cases is the same, i.e., a maximum of 4.5 years. Transition to the annular fuel with a 50% uprate would require changes in fuel manufacturing plants, since the enrichment increases to well above 5%. This is primarily the consequence of driving the fuel to 50% higher burnup, and similar enrichments would be required for solid fuel, if this fuel reaches such higher burnups. However, our preliminary calculations showed that if denser uranium compounds could be used, such as uranium nitride, and the number of reload assemblies can be increased, the fuel enrichment could be limited to 5 w/o.

	H	G	F	E	D	C	B	A
08	2L1670 0.897 0.920 61.948	0L1670 1.276 1.362 23.593	1L1670 1.132 1.186 45.079	0H2410 1.413 1.491 24.976	1L1670 1.143 1.185 46.816	0L1670 1.315 1.403 25.170	2H2410 0.957 1.093 42.938	2H2410 0.453 0.672 50.477
09	0L1670 1.276 1.362 23.593	2H1250 0.993 1.050 59.650	0L1610 1.328 1.413 24.608	2H1250 1.085 1.127 54.155	0H2410 1.380 1.475 25.006	1H2080 1.130 1.196 45.145	0H1670 1.191 1.361 21.575	2H1670 0.479 0.733 51.007
10	1L1670 1.132 1.186 45.079	0L1610 1.328 1.413 24.613	1L1670 1.093 1.132 46.799	0L1670 1.281 1.365 25.203	1L1610 1.036 1.115 45.242	1H1670 1.085 1.145 43.100	0H1670 1.186 1.351 21.614	2H1670 0.470 0.713 50.349
11	0H2410 1.413 1.491 24.976	2H1250 1.086 1.127 54.157	0L1670 1.281 1.365 25.202	1L1670 1.001 1.074 45.589	2H2410 1.003 1.049 45.458	1H1670 1.060 1.120 42.583	0H1250 1.085 1.290 20.200	2H2410 0.363 0.640 51.481
12	1L1670 1.143 1.185 46.816	0H2410 1.380 1.475 25.006	1L1610 1.036 1.115 45.243	2H2410 1.003 1.049 45.454	1H1250 1.093 1.205 41.106	0H2080 1.244 1.371 23.017	1H1250 0.749 1.039 33.655	
13	0L1670 1.315 1.403 25.170	1H2080 1.130 1.196 45.144	1H1670 1.085 1.145 43.098	1H1670 1.060 1.120 42.582	0H2080 1.244 1.371 23.016	0H1250 1.064 1.326 19.229	2H2080 0.391 0.718 51.647	
14	2H2410 0.957 1.093 42.938	0H1670 1.191 1.361 21.575	0H1670 1.186 1.351 21.614	0H1250 1.085 1.290 20.199	1H1250 0.749 1.039 33.655	2H2080 0.391 0.718 51.648		
15	2H2410 0.453 0.672 50.477	2H1670 0.479 0.733 51.006	2H1670 0.470 0.713 50.348	2H2410 0.363 0.640 51.478	Fuel ID Assembly power Peak pin power Assembly burnup			

Figure 9.15 EOC power distribution for reference core (WS).

Table 9.4 Reload fresh fuel enrichment levels and gadolinium loadings.

	WS	XF	XU
High enrichment (w/o)	4.80	5.70	8.10
Low enrichment (w/o)	4.40	4.95	9.00
Gadolinium amount (kg)	137.2	171.6	361.5

(1) 48 assemblies at high enrichment and 24 assemblies at low enrichment, gadolinium loading for all 72;
(2) enrichment for center portion (not including axial blankets).

	H	G	F	E	D	C	B	A
08	2L1210 0.937 0.967 64.167	0L0812 1.248 1.358 24.901	1L0812 1.121 1.185 48.411	0H1212 1.386 1.505 27.451	2H1212 1.099 1.163 61.052	0L1210 1.280 1.395 25.971	1L1210 0.940 1.075 45.276	1H1212 0.562 0.791 38.154
09	0L0812 1.248 1.358 24.901	2H0850 1.012 1.077 65.881	0L1210 1.302 1.411 25.980	2H0850 1.097 1.151 59.988	0H1610 1.360 1.469 27.211	1H1610 1.137 1.213 49.162	0H1280 1.182 1.355 23.526	2H1280 0.493 0.740 57.148
10	1L0812 1.121 1.185 48.411	0L1210 1.302 1.411 25.981	1L1212 1.108 1.159 48.879	0L1212 1.274 1.393 25.541	1L1210 1.039 1.126 48.703	1H1280 1.080 1.147 47.900	0H1280 1.166 1.339 23.689	2H1280 0.469 0.714 56.017
11	0H1212 1.386 1.505 27.451	2H0850 1.097 1.151 59.988	0L1212 1.274 1.393 25.540	1L1212 1.018 1.107 47.896	1H1610 1.023 1.080 50.259	1H1280 1.061 1.122 47.161	0H0850 1.066 1.276 22.423	2H1610 0.370 0.654 57.290
12	2H1212 1.099 1.163 61.052	0H1610 1.360 1.469 27.211	1L1210 1.039 1.126 48.703	1H1610 1.023 1.080 50.259	1H0850 1.113 1.234 45.168	0H1610 1.253 1.404 24.398	1H0850 0.750 1.040 37.324	
13	0L1210 1.280 1.395 25.971	1H1610 1.137 1.213 49.162	1H1280 1.080 1.147 47.900	1H1280 1.061 1.122 47.161	0H1610 1.253 1.404 24.398	0H0850 1.070 1.348 20.796	2H1610 0.407 0.744 56.627	
14	1L1210 0.940 1.075 45.276	0H1280 1.182 1.355 23.525	0H1280 1.166 1.338 23.688	0H0850 1.066 1.276 22.423	1H0850 0.750 1.040 37.324	2H1610 0.407 0.744 56.627		
15	1H1212 0.562 0.791 38.154	2H1280 0.493 0.740 57.148	2H1280 0.469 0.714 56.017	2H1610 0.370 0.654 57.290	Fuel ID Assembly power Peak pin power Assembly burnup			

Figure 9.16 EOC power distribution for 100%-power annular fuel core (XF).

	H	G	F	E	D	C	B	A
08	2L2410 0.968 1.001 94.289	0L2410 1.350 1.470 36.666	1L2880 1.124 1.175 73.573	0L2880 1.313 1.422 38.982	2H2880 0.947 0.988 102.349	0H2880 1.334 1.440 41.055	1L2410 0.980 1.111 67.491	2H2880 0.523 0.740 87.522
09	0L2410 1.350 1.470 36.666	2H2880 1.037 1.091 92.819	0L2880 1.332 1.441 38.588	2H2880 1.028 1.096 89.111	0H2880 1.302 1.412 40.261	1L2880 1.051 1.118 73.400	0H2880 1.267 1.425 36.963	1H2880 0.682 0.969 56.726
10	1L2880 1.124 1.175 73.573	0L2880 1.332 1.441 38.581	2H2880 1.004 1.053 93.549	0L3280 1.260 1.369 39.024	2H4060 0.926 1.018 88.512	1H2880 1.030 1.116 72.518	0H2880 1.259 1.418 37.033	1H4060 0.665 0.948 56.883
11	0L2880 1.313 1.422 38.982	2H2880 1.028 1.096 89.077	0L3280 1.259 1.367 38.976	1H1660 1.051 1.182 69.067	1L3280 0.912 0.962 72.097	1H2460 1.026 1.112 69.825	0H2460 1.127 1.346 34.212	2H2460 0.423 0.709 81.499
12	2H2880 0.947 0.988 102.349	0H2880 1.302 1.412 40.259	2H4060 0.925 1.018 88.548	1L3280 0.912 0.962 72.055	1H2880 0.988 1.084 75.714	0H4060 1.222 1.358 37.698	1H2880 0.726 0.968 62.357	
13	0H2880 1.334 1.440 41.055	1L2880 1.051 1.118 73.390	1H2880 1.030 1.116 72.519	1H2460 1.026 1.112 69.864	0H4060 1.223 1.359 37.755	0H1660 1.072 1.343 32.279	2H2880 0.395 0.694 86.882	
14	1L2410 0.980 1.111 67.491	0H2880 1.267 1.425 36.957	0H2880 1.259 1.417 37.035	0H2460 1.127 1.346 34.243	1H2880 0.729 0.970 62.465	2H1660 0.413 0.711 80.672		
15	2H2880 0.523 0.740 87.522	1H2880 0.682 0.969 56.713	1H4060 0.665 0.947 56.934	2H2460 0.423 0.709 81.545	Fuel ID Assembly power Peak pin power Assembly burnup			

Figure 9.17 EOC power distribution for 150%-power annular fuel core (XU).

The critical boron concentrations are compared in Figure 9.19, which is calculated at hot full power (HFP) with equilibrium xenon (Eq. Xe) conditions. All these cores achieve the same cycle length of 18 months. The maximum boron concentrations are comparable. The hot channel factor ($F_{\Delta h}$) and the hot spot factor (F_q) are compared in Figures 9.20 and 9.21. Note that the 150%-power core has a higher average heat generation rate. Nevertheless, the hot channel factors are fairly close for the three core designs. The hot spot factor of the 150%-power core is slightly worse than the other two, but still remains well within the limit.

Figure 9.18 Assembly fuel pin layouts for the annular-fueled, 150%-power PWR core

Figure 9.19 Core depletion comparison at HFP, Eq. Xe conditions.

Figure 9.20 Comparison of the hot channel factor.

Figure 9.21 Comparison of the hot spot factor.

The reactivity feedback parameters were also evaluated for all three cores. Table 9.5 summarizes these parameters. The moderator temperature coefficient of the annular fuel cores is slightly more negative than for the solid core. The Doppler coefficient is about the same, but the lower initial temperature of the annular fuel provides a larger margin for negative Doppler feedback in accidents with reactivity insertion and a smaller reactivity swing from zero to hot full power heat up. In general, the cores with annular fuel show similar behavior to conventional high energy cores. As the reload fuel enrichment increases, the neutron spectrum hardens, therefore the worth of control materials decreases. The same soluble boron concentration limit implies more reliance on the burnable poison control of excess reactivity. In addition, the preservation of shutdown margin is a challenging task. For the 150%-power core, the shutdown margin requirement will be violated at the EOC. Therefore, the control rod system needs to be revised. It is desired to have more control rods and stronger absorbers instead of traditional Ag-In-Cd alloy as control rod materials.

Overall, the annular fuel, high power density cores have comparable steady-state performance to conventional cores, including power distributions and reactivity coefficients. The control rod system needs to be revised to preserve the shutdown margin. The retrofittability of the new annular fuel is technically feasible for PWRs from a reactor physics point of view.

Table 9.5 Summary of reactivity feedback parameters*.

	WS	XF	XU
HFP, Eq Xe, Update Sm, ARO, critical			
ITC (pcm/°F)	−16.6	−20.9	−22.3
	−26.9	−31.5	−28.2
	−42.4	−45.2	−46.8
MTC (pcm/°F)	−15.1	−19.3	−20.7
	−25.4	−29.8	−26.6
	−40.7	−43.3	−45.1
Doppler (pcm/°F)	−1.53	−1.61	−1.54
	−1.53	−1.68	−1.61
	−1.61	−1.81	−1.76
Power coefficient (pcm/%)	−17.2	−12.4	−15.3
	−20.4	−15.4	−16.6
	−25.7	−20.6	−23.4
HFP to HZP (pcm)	1833	1298	1614
	2187	1717	1692
	2982	2429	2795
HZP, No Xe, Pk Sm, ARO, critical			
ITC (pcm/°F)	−4.47	−9.35	−10.7
	−11.4	−17.0	−14.3
	−21.9	−25.5	−26.2
MTC (pcm/°F)	−2.61	−7.63	−8.72
	−9.60	−15.2	−12.5
	−19.9	−23.6	−24.3
Doppler (pcm/°F)	−1.85	−1.72	−1.76
	−1.85	−1.79	−1.81
	−1.89	−1.91	−1.94
Power coefficient (pcm/%)	−23.8	−12.7	−17.7
	−26.3	−15.5	−15.2
	−38.3	−22.2	−30.1
* Three vertical numbers in each box represent BOC, MOC, EOC values, in that order.			

9.4 Fuel Cycle Considerations

The current industry practice of 18-month-cycles and 3-batch fuel management is assumed in all analyses. Since the annular fuel at 150% power, for the same cycle length, generates 50% more energy and accumulates 50% more burnup, the required reload fuel enrichment is beyond 5 w/o (a current licensing limit for several fuel manufacturers). In the long term, the 5 w/o limit will not be a barrier once the demand for enrichment above 5% increases to justify the one-time cost of modifying and re-licensing front-end fuel cycle facilities. However, in the short term, the enrichments above 5% are a hurdle to the

introduction of the annular fuel. Some possibilities exist in order to overcome the enrichment limit.

Based on the same UO_2 fuel, within the 5 w/o enrichment constraint, one has to make a decision whether to keep the 18 month cycle or to keep 3-batch fuel management. If the cycle length of 18 months is maintained, one can increase the number of reload fuel assemblies. In the extreme case, a single batch core is possible where all fuel assemblies are discharged at EOC. Or, if the number of reload fuel assemblies is fixed, the cycle length will be reduced (more frequent refueling). Nominally, an 18-month-cycle at 100%-power is equivalent to a 12-month-cycle at 150%-power level. This option has a better fuel utilization; but there are more refueling outages. Besides the fuel management choice, a more dense uranium loading is also helpful. For example, uranium nitride is a candidate compound.

9.5 Conclusions

Annular fuel is a very promising nuclear fuel concept for high power density cores allowing a power increases of up to 50% from the same size reactor vessel, while maintaining or even increasing reactor safety margins. Using CASMO-4 with fictitious increase of U-238 number densities has been identified to overcome the limitation of the current code version to adequately treat resonance integrals, and successfully applied in order to model this fuel with the CASMO-4/SIMULATE-3 package. SIMULATE-3 core calculations have shown that the key physics parameters are only mildly affected by this variation.

Lattice physics calculations based on MCNP quantified various neutronic differences between the annular fuel and the reference solid fuel. The change of the fuel geometry has the largest contribution to the eigenvalue changes. There are more U-238 epithermal captures in the annular fuel, which leads to a larger conversion ratio. The burnup capability of the solid fuel is about the same as the annular design for the same enrichment. However, the reduced fuel volume of annular fuel (by about 10% to accommodate additional cladding) implies an increased enrichment to match the same energy production as the reference solid fuel.

Equilibrium PWR core designs, for WS (solid reference core), XF (annular fuel core at 100%-power), and XU (annular fuel core at 150%-power) cores, are compared subject to the same set of design criteria. The design targets include 90% capacity factor, 1750 ppm maximum CBC, 1.65 maximum pin peaking, and 2.5 maximum hot spot factor. The steady-state core performance and reactivity feedback parameters were examined. Even though the annular fuel cores operate at lower fuel temperatures, the overall steady-state performance and reactivity feedback parameters are very similar to the reference solid fuel core. Hence, all the above design targets are satisfied.

Overall, the reactor physics analyses have confirmed that it is possible to design an annular fuel core for 150% power density that maintains the current 18-month cycle length, and generates 50% more energy. However, an enrichment above 5% (8-9%) is required to reach a burnup of 80-90MWd/kg, unless fuel with higher density and other measures to improve neutron economy are used. More burnable poison is needed than for the reference solid fuel to limit power peaking and soluble boron concentration.

10. EVALUATION OF PLUTONIUM BURNING IN FERTILE-FREE ANNULAR FUEL

Eugene Shwageraus, Pavel Hejzlar, and Mujid S. Kazimi

This chapter explores the basic feasibility of the high power density annular fuel design to eliminate Pu originating from spent LWR fuel. Application of the annular fuel concept is particularly attractive for such task because much higher power density is achievable with such fuel geometry resulting in faster rate of Pu destruction. The use of fertile free fuel matrix for hosting the Pu to be burnt offers additional advantage because it averts generation of additional Pu in contrast to conventional mixed oxide (MOX) fuel approach [Degueldre and Paratte, 1999, Lombardi and Mazzola, 1996, Chodak et al. 1997].

The objective of this work is to investigate feasibility of PWR core design fully loaded with annular fertile free fuel containing reactor grade Pu and operating at 150% of conventional PWR power density.

The potential fertile free fuel core design challenges are well known and reported in numerous publications [Degueldre and Paratte, 1999, Kloosterman and Damen, 1999, Kasemeyer et al. 1998]. Displacement of U238 with fertile free matrix results in less negative Doppler coefficient of the fuel, which is particularly important for mitigation of reactivity initiated accident consequences. This problem is typically solved by the use of burnable poisons, which absorb preferentially resonance neutrons (for example Er) or doping the fuel with small amounts of natural Uranium or Thorium [Lombardi, et al. 1999].

High thermal absorption of Pu hardens the neutron spectrum in comparison with a standard uranium core, and thus results in a reduction of reactivity worth of all the core reactivity control mechanisms. Moreover, a hard neutron spectrum may cause the moderator temperature coefficient (MTC) to become positive due to a particular structure of Pu isotopes' resonances. The possibility of a positive MTC imposes one of the major limitations on the Pu content in the fuel. [Kloosterman and Damen, 1999, Aniel-Buchheit et al., 1999].

Previously, fertile free fuel core analyses were reported in a number of publications. In [Lombardi and Mazzola, 1996] a 3-dimensional simulation of AP-600 core was performed. In the proposed fuel assembly configuration, about 21% of the UO₂ fuel rods were displaced with Reactor grade Pu rods in fertile free matrix. The core operated at somewhat lower power density (13.5 kW/m) than a typical (4-loop 1100MWe) PWR, in 3-batch fuel management scheme, and about 400 Effective Full Power Days (EFPD) fuel cycle length. An extensive comparison of various Weapons (WG) and Reactor Grade (RG) Pu containing cores including 100% fertile free cores was reported in [Kasemeyer et al. 1998]. The reference reactor was 900 MWe PWR operating in annular fuel cycle in 4-batch fuel management scheme.

In both references, the standard solid fuel pin geometry was assumed. Additionally, the combination of the parameters such as power density, fuel cycle length, and fuel

management scheme suggests that the reported fertile free cores require smaller Pu loading than the US industry standard PWR with 18 month fuel cycle and 3-batch fuel management. This implies that the reported fertile free core design challenges are less pronounced than what would be the case for the US standard PWR if it were to adopt the full fertile free Pu core.

Moreover, in the high power density (150% of the nominal) annular fuel case, the Pu loading would have to be additionally increased by at least 50% if the fuel cycle length is to be preserved. Such a high Pu loading in turn, would require a large amount of burnable poison to compensate for the excess reactivity of the fuel. Both, Pu and burnable poisons, being primarily thermal neutron absorbers, would further harden the neutron spectrum and therefore cause even larger reduction in reactivity worth of all the control materials and have a greater potential for positive void and moderator temperature coefficients.

The current analysis is an attempt to address some of the issues related to the high power density fertile free cores with annular internally cooled fuel geometry. In this work, we consider only Reactor Grade Pu as it appears after reprocessing of a spent LWR fuel.

10.1 Computational Methods and Benchmarking

In this analysis, the same Studsvik Core Management System (CMS) as in Chapter 9 was used. The system includes the two dimensional assembly burnup code CASMO-4 [Edenius et al., 1995] used for the preparation of burnup dependant cross-sections, TABLES-3 [Verplanck, 1995] – the auxiliary code for the cross-section processing, and 3-dimensional nodal diffusion code SIMULATE-3 [Digiovine et al 1995] used for the full core analysis.

In the evaluation of UO₂ annular fuel, a limitation of CASMO4 computer code treatment of resonance absorption self-shielding to solid fuel pin geometry was overcome by artificial adjustment of U238 number densities to correct the reaction rates in U238 and buildup of Pu isotopes, as discussed in Chapter 9. The state of affairs is somewhat more complex in the case of annular fertile free fuel because such an adjustment of number densities is no longer possible in the absence of major resonance absorber such as U238. On the other hand, the resonance (Pu) nuclides are diluted to a large extent by the fertile free matrix atoms and their concentration is much lower than in typical UO₂ fuel making the fuel much more transparent for neutrons. As a result, the effect of resonance self-shielding in the fertile free annular fuel case is considerably smaller than in the case of annular UO₂ fuel.

A series of benchmark calculations were performed in order to estimate the magnitude of any deviation due to using the CASMO4 code for criticality and isotopic number densities evolution with burnup in annular fertile free fuel calculations. The CASMO code results were compared with those of MCODE [Xu et al., 2002], where the annular fuel pin geometry is represented explicitly and the neutron transport problem is solved by the Monte Carlo method [Briesmeister, 2000], while the burnup problem is solved with the ORIGEN2 computer code [Croff, 1980]. Both solid and annular fuel pin geometries were considered as well as the higher Pu loading and power density for the annular case. The list and a description of considered benchmark cases are shown in Table 10.1.

Table 10.1 Description of Benchmark Cases

Case	Computer Code	Geometry	Pu loading, vol. % PuO ₂	Power Density, W/cm ³
1	CASMO	Solid pin	7.0	104
2	CASMO	Annular pin	7.0	104
3	CASMO	Annular pin	10.5	157
4	MCODE	Solid pin	7.0	104
5	MCODE	Annular pin	7.0	104
6	MCODE	Annular pin	10.5	157

Good irradiation performance, and favorable mechanical and thermal properties are of crucial importance in the choice of fuel matrix material. Whereas, the type of fertile free matrix material is expected to have only limited impact on the overall neutronic characteristics of the fuel.

In the current analysis, we assumed a dispersion type fuel where fissile fuel microparticles are dispersed in a fertile free host matrix. The microparticles consist of a homogeneous mixture of PuO₂ and Yttria Stabilized Zirconia (YSZ) and sized to confine radiation damage. The host matrix was assumed to be a 1:1 mixture of MgO and ZrO₂. Such a mixture is expected to have good mechanical and thermal properties [Medvedev et al 2005]. National and international research efforts aiming at the development of suitable fertile free fuel matrix materials are still ongoing, which implies that other fertile free fuel concepts or materials may be found preferable to those used in the current analysis. However, as mentioned earlier, the fuel matrix material is expected to have only marginal impact on the neutronic characteristics of the fuel.

The benchmark calculations were performed for a single fuel unit cell with reflective boundary conditions. The unit cell geometry, fuel composition, and operating conditions are summarized in Table 10.2. In both, solid pin and annular pin cases, the fissile microparticles structure was neglected and the fuel was represented as homogeneous mixture of micro-particles and fuel matrix components. The effect of such simplification on the fuel criticality and isotopic evolution is small (on the order of fraction of a percent for criticality) as demonstrated in [Shwageraus et al., 2005].

Pu isotopic composition assumed in this analysis corresponds to that of Pu from a typical spent LWR with initial uranium enrichment of 4.5%, 50 MWd/kg discharge burnup, after 10 years of cooling. The Pu isotopic vector and mass densities of all the fuel components are listed in Table 10.3.

Additionally, a 3000 MW_{th} PWR core fully loaded with Pu fertile free fuel in solid pin geometry, described in [Kasemeyer et al. 1998], was modeled with Studsvik CMS code. Whereas, the calculations reported in reference [Kasemeyer et al. 1998] were performed at Paul Scherrer Institute (PSI) in Switzerland with ELCOS code system [Paratte et al., 1988]. Although, only a limited number of parameters were available for comparison and despite some uncertainty in the initial data, the calculations performed with the Studsvik CMS code were in very good agreement with the PSI results. The obtained discrepancy

in the core critical boron concentration as a function of burnup, fuel cycle length, and the fuel isotopic balance predicted by both computer code systems is within a few percent confirming the used calculation methodology.

Table 10.2 Benchmark Assumptions Summary

	Solid Pin	Annular Pin
Fuel Pin Pitch, cm	1.2600	1.6510
Fuel Pin Outer Diameter, cm	0.95	1.5367
Cladding Thickness, cm	0.0573	0.0573
Gap Thickness, cm	0.0082	0.0082
Fuel Pin Inner Diameter, cm	n/a	0.8633
Fuel Matrix Volume Fraction	0.7000	0.7000
Fuel Matrix Composition	MgO-ZrO ₂ (1:1 vol.)	MgO-ZrO ₂ (1:1 vol.)
Fissile Particles Composition	PuO ₂ -YSZ	PuO ₂ -YSZ
Y ₂ O ₃ mol./o in YSZ	8.0	8.0
Cladding Material	Zircaloy-4	Zircaloy-4
Coolant Temperature, K	583.1	583.1
System Pressure, MPa	15.5	15.5
Fuel Temperature, K	900	900

Table 10.3 Fuel Composition Summary

Fuel Component Parameter		Value
MgO density, g/cm ³		3.65
ZrO ₂ density, g/cm ³		5.68
Y ₂ O ₃ density, g/cm ³		5.03
PuO ₂ density, g/cm ³		10.77 (94% of theoretical)
Pu isotopic composition, w/o	Pu238	3.18
	Pu239	56.35
	Pu240	26.62
	Pu241	8.02
	Pu242	5.83

10.1.1 Benchmark Results

The results of the benchmark are reported in Figures 10.1 through 10.5. Figure 10.1 compares the annular and solid fuel pin cases calculated at standard PWR power density by MCODE and CASMO computer codes. Similar to UO₂ fuel case, the transition from solid to annular fuel pin geometry reduces the initial criticality of the fuel. This is a result of a combination of several neutronic effects such as changes in resonance absorption due to increased surface area of the fuel and additional absorption in the cladding. Further investigation is required to identify the specific nature of this reduced criticality effect. The reduction in initial fuel criticality due to the transition from solid to annular geometry is on the order of 2%, which is somewhat lower than in the case of UO₂ fuel. About the same order of relative reduction in fuel criticality is predicted by CASMO and MCNP computer codes. However, for both fuel geometries, CASMO over predicts the fuel criticality value by about 1% as compared to MCNP. This may be partially attributed to under prediction of the absorption reaction rates in Pu242 by the CASMO code due to limitations of the standard 70 energy group cross section library used by the CASMO, in particular the resonance peak at 2.0eV, which is not covered well with the 70 groups. It should also be noted that the CASMO cross-section library is based on JEF-2 evaluated data file whereas ENDF/B-VI library was used for the MCNP runs potentially providing additional source of discrepancy.

Both CASMO and MCODE predict similar trend for the fuel criticality change with burnup. The criticality of annular fuel becomes higher than that of the solid fuel after 300 to 400 MWd/kg of burnup. CASMO predicts somewhat steeper criticality reduction with burnup in both annular and solid fuel cases than MCODE. The fuel cycle length based on the CASMO calculation is underestimated and therefore is considered conservative.

Similar trends can be observed also from Figure 10.2, which shows the same criticality curves as in Figure 10.1 but with the burnup presented in terms of Effective Full Power Days (EFPD) at a standard PWR power density of 104 W/cm³. Here, again, the single batch discharge fuel burnup (B1) predicted by CASMO is lower than that predicted by the MCODE by about 5% in the annular fuel case and by up to 10% in the solid fuel case. Longer fuel cycle length can be expected from the solid fuel due to the 14% larger fuel volume (on a pin cell basis) of the solid compared to annular fuel designs. However, on an assembly average basis, the annular 13×13 fuel assembly would have less heavy metal by only about 10% than in the standard 17×17 solid pins fuel assembly due to the lower number of guide tube locations in the annular fuel assembly.

Criticality of the annular fuel vs. burnup at 50% higher than nominal PWR power density (Cases 3 and 6 from Table 10.1) as calculated by CASMO and MCODE are compared with annular fuel criticality at nominal power density in Figures 10.3 (MWd/kg) and 5 (EFPD).

The initial k_{∞} of the high power density fuel is slightly (by 0.1 to 0.2%) higher than for the nominal power density fuel due to the higher fissile Pu loading and therefore some changes in the neutron spectrum. The k_{∞} gradually becomes lower for high power density fuel due to the higher equilibrium Xe concentration than in the nominal power density core. The overall power density effect on the fuel criticality is generally small, as can be observed from both CASMO and MCODE results. This is likely due to the harder

neutron spectrum in the high power density (high Pu loading) fuel and therefore lower Xe reactivity worth. It is also noted that the power density effect is lower in a Pu core than in a uranium core due to the lower Xe fission yield of Pu239 as compared with U235.

Figure 10.1 K-infinity vs. Burnup (MWd/kg): Solid and Annular Pins Comparison.

Figure 10.2 K-infinity vs. Burnup (EFPD): Solid and Annular Pins Comparison.

Figure 10.3 K-infinity vs. Burnup (MWd/kg) at Nominal and 150% Power Density.

Figure 10.4 K-infinity vs. Burnup (EFPD) at Nominal and 150% Power Density.

Figure 10.5 Evolution of Pu Isotope Concentrations with Burnup.

In the high power density case, the CASMO prediction of annular fuel criticality also decreases faster with burnup than that predicted by the MCODE and is therefore conservative.

Figure 10.5 compares the evolution of Pu isotopes with burnup for the annular fuel calculated with both CASMO and MCODE (Case 2 and 5 respectively). The agreement in isotopic densities prediction between the two codes is very good despite the fact that the CASMO code was not designed to handle the annular fuel geometry. The difference in Pu242 isotope concentration prediction however, may reach up to 10% at fuel discharge. As mentioned earlier, this is most likely due to the structure of CASMO cross section library. More fine energy group structure may be required in order to obtain better agreement in the Pu242 reaction rates. As mentioned earlier, an additional reason may be the fact that CASMO4 cross section library used for this analysis is based on JEF2.2 evaluated data file whereas ENDF-B/VI data was used in the MCNP (MCODE) calculations.

Overall, we concluded that CASMO4 is well suited for preliminary neutronic analysis of Pu loaded fertile free annular fuel without any artificial changes in fuel cell parameters to overcome the code limitation to handle solid fuel pin geometry only. Fair agreement was observed between CASMO4 and MCODE results in all high and nominal power density annular fuel cases in the prediction of fuel criticality and Pu isotopes concentrations. This is in contrast to the annular UO_2 fuel, where the effect of annular geometry on U238 absorption was considerably larger and artificial adjustment of U238 number density in CASMO was required match MCODE predictions. The small magnitude of annular geometry effect in the fertile free fuel case is attributed to the considerably lower

concentration of resonance nuclides, which are diluted to a great extent by the fertile free matrix atoms. Therefore, the spatial resonance self shielding effect is small.

10.2 Burnable Poison and General Design Considerations

In comparison with a standard UO_2 PWR core, the Pu loaded fertile free core has significantly different neutronic characteristics which may affect the potential feasibility of the Pu cores. Most important among them are

- Significantly reduced Doppler Coefficient (DC) due to the fact that U238, which is responsible for most of the Doppler Effect in UO_2 fuel, is displaced by a fertile free matrix.
- Harder than in UO_2 core neutron spectrum, due to the higher absorption cross section of Pu isotopes (particularly Pu239) compared to U235 absorption cross section.

A reduced Doppler coefficient would negatively affect transient behavior of the core especially in fast transients such as control rod ejection, where energy deposition in the fuel and consequent fuel damage threshold are roughly inversely proportional to the magnitude of the Doppler Coefficient.

A harder neutron spectrum leads to considerably lower reactivity worth of control materials (control rods, soluble boron, and burnable poisons). In addition, a harder neutron spectrum may result in positive void (VC) and moderator temperature coefficients (MTC).

A typical way to improve DC is by the use of Er as a burnable poison. Neutron capture resonances of the Er167 isotope strongly overlap with fission resonances of Pu239 as illustrated in Figure 10.6. Therefore, an increase in the fuel temperature would result in increased absorption in Er167 simultaneously with the increased fission rate in Pu239 mitigating the positive component of DC. However, natural Er, besides isotope Er167, contains several other isotopes with small but not negligible absorption cross section. Consequently, the use of natural Er as burnable poison results in a significant residual amount of poison, an end of cycle reactivity penalty, and eventually reduced discharge fuel burnup. Moreover, higher fissile loading would be required to compensate for the reactivity penalty due to residual burnable poison and to preserve the desired fuel cycle length. In turn, higher Pu loading would result in further hardening of the neutron spectrum which increases the undesirable effects mentioned above.

Alternative solutions would be the use of Er enriched in Er167 isotope or the use of Gd. In both cases, the residual poison reactivity penalty would be considerably smaller than in the natural Er case. The natural Gd residual reactivity penalty and the amount of BP required can be further reduced by the use of Gd enriched in Gd157 isotope.

One of the Gd advantages is its particularly high thermal absorption cross section, which can permit additional flexibility in fuel assembly burnable poison design. If concentrated in a small number of fuel pins, Gd exhibits the spatial self-shielding effect allowing for its very gradual burnout. Variation in the number of Gd loaded pins allows for control over the amount of reactivity being suppressed, while variation of Gd loading per pin allows for control over the burnout time of the poison. A disadvantage of such a BP

design strategy is that it generally results in a much higher pin power peaking as compared to designs where BP is spread over larger number of fuel pins. In addition, the thermal cross section ratio to the resonance integral of Gd157 isotope is much higher than that of Er167 isotope. Therefore, Gd is not expected to improve the Doppler Coefficient of the fertile free fuel.

Figure 10.6 Pu239 Fission and Er167 Absorption Cross Sections

At the preliminary design stage of the current analysis, we performed a series of simple fuel assembly level evaluations of different burnable poisons for the design of fully fertile free Pu-loaded core operating at 150% of the standard PWR power density. We compared enriched and natural Gd and Er cases with the no burnable poison case. The parameters of interest were:

- Single batch reactivity limited burnup (B1), as an indicator of the residual poison reactivity penalty
- BP loading required to suppress certain amount of excess reactivity
- Pin power peaking factor
- Reactivity feedback coefficients (DC, MTC, Void Coefficient)
- Soluble Boron reactivity worth (BW)

B1 is defined here as the fuel burnup at which a single batch loaded core will reach the level of zero reactivity (core leakage reactivity was neglected for simplicity).

The annular fuel 13×13 with 9 guide thimbles assembly configuration was used as the reference design. The assembly geometry corresponds to the optimal thermal hydraulic design of the annular UO₂ fuel reported in Chapters 6 and 9. The fuel composition and geometry are also identical to those used for the benchmark exercise described earlier and presented in Tables 10.2 and 10.3.

In all cases, PuO₂ loading and core power density were 10.5 v/o and 157 W/cm³ respectively. Enriched Er was assumed to contain 80 at.% of Er167, whereas the ratio among the rest of the Er isotopes was maintained as in natural Er. The same assumption was made concerning Gd157 isotope. The isotopic compositions of natural and enriched Er and Gd used for this analysis are presented in Table 10.4. The BP loading was chosen such that the fuel BOL k-infinity would be about the same for all cases considered and equal to approximately 1.08. In the cases with natural and enriched Gd, the BP was concentrated in 44 fuel pins (out of total 160 pins). In the Er cases, all fuel pins in the assembly contained the same amount of BP.

Table 10.4 Er and Gd Isotopic Compositions.

Isotope	Natural (at.%)	Enriched (at.%)	Isotope	Natural (at.%)	Enriched (at.%)
Er-162	0.14	0.04	Gd-152	0.2	0.05
Er-164	1.61	0.42	Gd-154	2.18	0.52
Er-166	33.6	8.72	Gd-155	14.8	3.51
Er-167	22.95	80.00	Gd-156	20.47	4.85
Er-168	26.8	6.96	Gd-157	15.65	80.00
Er-170	14.9	3.87	Gd-158	24.84	5.89
			Gd-160	21.86	5.18

The results of the assembly level calculations are presented in Table 10.5 and in Figures 10.7 and 10.8.

Table 10.5 Summary of Assembly Level Calculation.

Case	PuO ₂ loading, vol. %	BP loading, vol. %	BP mass, kg/assy	B1, MWd/kg	Max. pin power peak
No Burnable Poison	10.5	-	-	528	1.12
Natural Er	10.5	3.5	12.26	442	1.12
Enriched Er	10.5	1.2	4.20	510	1.11
Natural Gd	10.5	10.0	8.45	511	1.22
Enriched Gd	10.5	5.0	4.22	537	1.20

Figure 10.7 K-infinity vs. Burnup for Various Burnable Poisons.

Figure 10.8 Pin Power Peaking Factors vs. Burnup for Annular Fuel Assemblies with Various Burnable Poisons.

The following observations are made.

- BP enrichment significantly reduces the amount of poison required to suppress the excess fuel reactivity
- BP enrichment significantly reduces the negative reactivity penalty, thus increasing achievable fuel burnup
- Natural Er results in the highest residual reactivity penalty, reducing B1 by almost 20%
- Enriched Gd, on the other hand, provides even some reactivity “bonus” resulting in slightly higher B1 than the no BP case, likely because of the changes in neutron spectrum and thus, more efficient conversion of fertile Pu isotopes into fissile isotopes.
- Er requires somewhat lower loading by volume due to the fact that it is evenly distributed over all fuel pins in the assembly and not lumped into few pins as Gd and also due to the slightly higher density of Er_2O_3 than Gd_2O_3 . A lower volumetric fraction of BP is considered beneficial because the BP material admixture effect on mechanical, thermal, and irradiation properties of the fuel matrix is largely unknown and would require additional investigation if the percentage of BP in the matrix is particularly high.
- Assembly pin power peaking is considerably higher in Gd cases due to the lumping of BP into few pins

The results of the reactivity coefficients evaluation are summarized in Table 10.6. The data for standard UO_2 solid fuel pin PWR fuel is also shown in Table 10.6 for comparison. The reactivity coefficients were evaluated at 3 burnup points of 0, 350, and 700 MWd/kg, approximately simulating BOL, MOL, and EOL burnup of fertile free fuel cases respectively. UO_2 fuel reactivity coefficients were evaluated at 0, 25, and 50 MWd/kg.

In order to simulate the actual core average conditions, the reactivity coefficients were evaluated at 1000, 500, and 0 ppm of soluble boron in the coolant for BOL, MOL, and EOL burnup points respectively.

The following observations are made.

- All fertile free fuel cases exhibit a reduction in magnitude of the Doppler Coefficient by more than a factor of 2 in comparison to the UO_2 fuel
- Use of Er slightly improves the DC by 20 to 30% in comparison with the no BP case
- Use of Gd has no effect on DC
- The BOL MTC and VC are positive for the enriched and natural Gd cases whereas the same coefficients for the Er cases are comparable to the standard UO_2 fuel
- The BOL boron reactivity worth is significantly reduced (by almost a factor of 3) in comparison with the UO_2 fuel, as expected

- The rate of increase in boron worth with burnup is much higher in fertile free fuel cases than in the UO₂ fuel case due to the rapid burnout of Pu and BP in contrast to UO₂ fuel where Pu is continuously generated from U238.
- Use of Er is preferred over the use of Gd as it results in slightly higher BOL boron worth (by about 10%)

Table 10.6 Reactivity Coefficients and Soluble Boron Worth

		UO ₂	No BP	Nat. Gd	Nat. Er	Enr. Gd	Enr. Er
DC, pcm/K	BOL	-2.2	-1.0	-1.0	-1.2	-1.0	-1.3
	MOL	-2.9	-1.0	-1.1	-1.1	-1.0	-1.1
	EOL	-3.3	-1.4	-1.5	-1.4	-1.4	-1.4
MTC, pcm/K	BOL	-22	-11	6	-25	11	-20
	MOL	-52	-27	-24	-41	-22	-35
	EOL	-78	-48	-50	-68	-49	-54
VC, pcm/%void	BOL	-63	-25	13	-56	28	-44
	MOL	-146	-78	-74	-113	-67	-95
	EOL	-228	-192	-201	-246	-196	-206
BW, pcm/ppm	BOL	-4.8	-1.7	-1.7	-1.9	-1.7	-1.9
	MOL	-5.2	-2.9	-2.8	-3.0	-2.8	-3.0
	EOL	-6.2	-14.4	-14.1	-14.4	-14.1	-14.3

10.3 Consideration of Void Reactivity Effects

One of the concerns in Pu loaded fertile free cores is the void reactivity feedback. Although the reactivity void coefficient might be negative at low coolant void fractions, it may be positive at high void fraction. Therefore, in a highly unlikely event of a total core drainage without reactor scram or significant coolant voiding in an isolated region of the core, the void fraction may reach 90% or more resulting in positive reactivity addition and even super-critical core. This effect was shown to set a limit for the Pu loading in the case of full MOX PWR core [Aniel-Buchheit et al. 1999] and investigated in [Zhang, 2003]

This section reports the results of a simple void reactivity effect analysis based on assembly level calculations. The increasing neutron leakage effect with increasing coolant void fraction was taken into account by assuming that the core leakage reactivity is proportional to the core surface to volume ratio and to the neutron migration length for the case of a “drooping ends” core radial power profile [Driscoll et al., 1990].

$$\rho_{\text{leakage}} = \frac{S}{V} \times \frac{M}{4} \quad (10-1)$$

We considered the annular fertile-free fuel with 7, 9, 11, 13, and 15 vol.% PuO₂ loading without any BP.

Also, the 15% Pu loading case was calculated with the use of BP. A combined BP design was adopted in order to take advantage of both enriched Er and enriched Gd properties. The assembly consisted of 56 enriched Gd pins with 5 vol. % loading and the remainder of the fuel pins contained 0.4 vol.% of enriched Er.

All calculations were performed at zero ppm of boron in the coolant.

The results of the void reactivity effect evaluations are presented in Figures 10.9 through 10.11.

Figure 10.9 shows the corrected for leakage fuel reactivity versus coolant void fraction at BOL. Only the 7% Pu loading case exhibits negative VC up to 100% of the void. The positive void begins at lower void fractions as the Pu loading increases. For the 15% loading and no BP case the positive VC region starts already at 50% void fraction.

Figure 10.9 Fertile Free Fuel Reactivity vs. Void Fraction at BOL.

The obtained results are consistent with those reported in [Kasemeyer et al. 1998] for the comparable RG Pu loadings and void fractions. The VC results reported previously are negative and therefore acceptable due to the fact that relatively low Pu fuel content was sufficient for the modeled core and the fuel cycle.

Figure 10.10 Reactivity vs. Void Fraction, 15v/o PuO₂ Fuel no BP.

Figure 10.11 Reactivity vs. Void Fraction, 15v/o PuO₂ Fuel with BP.

In the case of the fuel with BP, the void coefficient is positive all the way from zero to 100% void fraction. This is a result of the fact that burnable poisons are mostly thermal neutron absorbers whose effectiveness is reduced with increasing void fraction and associated hardness of the spectrum.

Figures 10.10 and 10.11 show the change in void reactivity with fuel burnup for 15% PuO₂ cases without and with BP respectively. Generally, the VC becomes less positive and eventually negative, even at high void fractions, for both cases primarily due to depletion of Pu isotopes and accumulation of fission products. In the case of the fuel with BP, the VC becomes negative for all void fractions at about 300 to 400 MWd/kg burnup, which indicates that in multi batch core, the core average void coefficient may still be negative even at BOC and for all void fractions.

A more thorough investigation, such as full core Monte Carlo simulation, is required in order to confirm this anticipation.

10.4 Full Core Design Options

This section reports the results of a 3-dimensional full core simulation of possible fertile free Pu loaded annular fuel designs. This part of the study had the following design objectives and constraints:

- At least 450 Effective Full Power Days fuel cycle length, which corresponds to industry standard 18 months cycle.
- 3-batch fuel management scheme.
- All negative reactivity feedback coefficients.
- Critical boron concentration below 2000 ppm at all times.
- Maximum core radial power peaking factor below 1.5.

Standard Westinghouse PWR core parameters were used for the analysis. The core and fuel assembly design data are summarized in Table 10.7.

Tables 10.8 and 10.9 describe some of the calculated cases. A standard PWR UO₂ fuel case at nominal PWR power density as well as a fertile-free Pu loaded core with solid fuel pins also at nominal power density were modeled for comparison purposes.

The annular fuel cases described in Table 10.8 are the most representative of numerous trials attempting to achieve all the design objectives mentioned above while satisfying all the design constraints.

Cases 1 and 2 represent solid fuel pellet, nominal power density reference UO₂ and full fertile free Pu cores respectively. Cases 3 through 7 feature annular fuel geometry evaluated at 150% of the nominal PWR power density.

The major design challenge was the achievement of the required fuel cycle length while keeping the BOC critical boron concentration below 2000ppm. The difficulty arises from the fact that for the annular fuel core operating at 150% power density, the Pu loading has to be at least 1.5 times that of the solid pellet Pu core, in order to maintain the same 18 months fuel cycle length. A higher Pu loading requires a higher BP loading to compensate for the initial fuel excess reactivity. High BP and Pu loadings, in turn, result

Table 10.7 Standard Westinghouse PWR Core Parameters

<i>Core description</i>	<i>Solid pins</i>	<i>Annular Pins</i>
Power density, W/cm ³	104.5	156.8
Primary system pressure, MPa	15.5	15.5
Total core flow rate, Mg/sec	18.63	27.95
Core inlet temperature, °C	292.7	292.7
<i>Fuel Rod</i>		
Total number of fuel rod locations	50,952	30,880
Pellet outer diameter, mm	8.192	10.41
Pellet inner diameter, mm	-	9.90
Gap thickness, mm	0.082	0.062
Cladding material	Zircaloy-4	Zircaloy-4
Cladding thickness, mm	0.572	0.572
Outer cladding outer diameter, mm	9.5	15.37
Inner cladding inner diameter, mm	-	8.63
Active fuel height, m	3.66	3.66
<i>Fuel Assembly</i>		
Total number of fuel assemblies	193	193
Lattice type	Square	Square
Assembly lattice geometry	17 × 17	13 × 13
Number of fuel rods locations per assembly	264	160
Number of grids per assembly	7	7
Fuel rod pitch, cm	1.26	1.651
Assembly dimensions, cm	21.5 × 21.5	21.5 × 21.5

in significantly harder neutron spectrum and therefore lower reactivity worth of soluble boron. Low BW mandates very careful BP design in order to construct the fuel reactivity vs. burnup curve that would fulfill the following two requirements:

- Very small deviation of reactivity from the core average critical value of $\rho = 0$. The reactivity curve should be relatively flat in order to maintain the 3-batch average core reactivity below 2000ppm of critical boron concentration. Small deviations are required because even small variation in reactivity result in a large variation in boron concentration because of the very low reactivity worth of boron.
- On the other hand, the fuel reactivity curve should not be too flat. In that case, all fuel batches in the core would have nearly the same reactivity resulting in almost

a cosine shaped radial power distribution and therefore unacceptably high power peaking.

Table 10.8 Description of the Full Core Simulation Cases.

Case No.	Core Power, MW	PuO ₂ Loading, v/o	Description
Case 1	3411	4.2*	Reference UO ₂ Core, Solid IFBA pins, 17×17 assembly
Case 2	3411	11.5	Solid pins, 17×17, Natural Er BP
Case 3	5117	20.0	Annular pins, 13×13, Enriched Er + Natural Gd BP
Case 4	5117	20.0	Annular pins, 13×13, Enriched Er + Enriched Gd BP
Case 5	5117	15.0	Annular pins, 13×13, Enriched Er + Enriched Gd BP
Case 6	5117	16.5	Annular pins, 13×13, Enr. Er + Enr. Gd BP, 16 water holes
Case 7	5117	15.0	Annular pins, 13×13, Enr. Er + Enr. Gd BP, 16 water holes

* Uranium enrichment (w/o U235)

Table 10.9 Summary of Burnable Poison Designs.

Case No.	BP type	Gd ₂ O ₃ loading			Er ₂ O ₃ loading			Boron loading, kg/assy
		vol. %	pins/assy	kg/assy	vol. %	pins/assy	kg/assy	
1	Nat. Boron IFBA	-	-	-	-	-	-	3.411
2	Nat. Er	-	-	-	4.0	264	15.385	-
3	Enr. Er + Nat. Gd	9.0	60	10.372	2.6	100	5.684	-
4	Enr. Er + Enr. Gd	3.5	68	4.564	2.2	92	4.425	-
5	Enr. Er + Enr. Gd	4.0	68	5.216	0.3	92	0.603	-
6	Enr. Er + Enr. Gd	5.0	56	5.370	0.4	88	0.770	-
7	Enr. Er + Enr. Gd	4.0	56	4.296	0.3	88	0.577	-

All annular fuel cases use somewhat complex BP designs with both Gd and Er fuel pins. This allows flexibility, necessary for construction of reactivity curves of the desired shape.

In Cases 6 and 7, 10% of the fuel pins are replaced with empty water holes in order to soften the neutron spectrum and achieve higher soluble boron worth. Case 6 has the same Pu inventory as Case 5 achieved by 10% higher Pu volume % to compensate for the 10% reduction in the number of fuel pins per assembly. Whereas Case 7 has the same volume % of PuO₂ in the fuel as in Case 5, therefore 10% lower heavy metal mass per assembly.

Fuel loading patterns were designed keeping in mind the above considerations. In addition, fertile free Pu loaded cores have generally higher leakage than UO₂ core resulting in very low power in the peripheral fuel assemblies. Therefore, the strategy of

placing twice burned fuel assemblies in the core periphery was found beneficial with regards to reducing neutron leakage without significant increase in the core power peaking.

The core fuel loading patterns for all calculated cases are presented in Figure 10.12.

C	A	C	C	A	C	A	B
A	C	C	A	C	A	B	B
C	C	A	C	A	C	A	B
C	A	C	A	C	A	B	B
A	C	A	C	A	C	B	
C	A	C	A	C	B	B	
A	B	A	B	B	B		
B	B	B	B				

Case 1

C	C	C	C	A	C	A	B
C	C	C	B	C	A	A	B
C	C	C	C	A	C	A	B
C	B	C	A	C	A	B	B
A	C	A	C	B	A	B	
C	A	C	A	A	A	B	
A	A	A	B	B	B		
B	B	B	B				

Case 2

A	A	A	A	B	A	B	C
A	A	A	C	A	B	B	C
A	A	A	A	B	A	B	C
A	C	A	B	A	B	C	C
B	A	B	A	C	B	C	
A	B	A	B	B	B	C	
B	B	B	C	C	C		
C	C	C	C				

Case 3

A	A	B	A	C	A	A	B
A	C	A	B	A	A	B	C
B	A	C	A	C	A	B	B
A	B	A	B	A	B	C	C
C	A	C	A	C	B	C	
A	A	A	B	B	B	C	
A	B	B	C	C	C		
B	C	B	C				

Case 4

A	B	C	A	A	A	B	C
B	C	B	C	A	B	A	C
C	B	C	A	B	A	B	C
A	C	A	B	A	B	B	C
A	A	B	A	A	A	C	
A	B	A	B	A	B	C	
B	A	B	B	C	C		
C	C	C	C				

Case 5

A	A	C	A	B	A	B	C
A	C	A	C	A	B	A	C
C	A	C	A	B	A	B	C
A	C	A	B	A	B	B	C
B	A	B	A	A	B	C	
A	B	A	B	B	B	C	
B	A	B	B	C	C		
C	C	C	C				

Cases 6 and 7

Figure 10.12 Core Loading Maps (A-Fresh, B-Once, C-Twice)

In the process of modeling the equilibrium core conditions for each considered case, it was assumed that all partially burnt assemblies within each batch have the same burnup, equal to the average EOC burnup of the previous batch. This is a conservative assumption with respect to power peaking because variation of burnup of individual assemblies within the batch allows some flexibility in mitigating the power peaking by placing the less reactive assemblies in the high power core regions.

The core reactivity coefficients were evaluated at Hot Full Power (HFP), all-rods-out, Xe equilibrium conditions.

Fertile free annular Pu core characteristics are compared with those of the standard UO₂ core in Figures 10.13 through 10.17 and with the solid fuel pellet fertile free core in Figures 10.18 through 10.22.

Figure 10.13 Critical Boron Concentration: Reference UO₂ vs. FFF Cores Comparison.

Fuel batch average burnup values at equilibrium for each calculated case are reported in Table 10.10. Since all annular fuel cases considered are capable of achieving about the same fuel cycle length of 450 EFPD (Figures 10.13 and 10.18), the discharge fuel burnup (column “Twice” in Table 10.10) is inversely proportional to initial heavy metal inventory. Case 3 has lower discharge burnup than Case 4 partly due to residual reactivity penalty of natural Gd used in that case. Case 7 with the lowest initial Pu inventory is capable of achieving the highest discharge burnup of about 600 MWd/kg, which corresponds to approximately 63% of Pu destruction per pass through the core. This is about 25% higher than for the solid pin fuel case (Case 2).

Figure 10.14 Radial Core Power Peaking: Reference UO2 vs. FFF Cores Comparison.

Figure 10.15 Soluble Boron Worth: Reference UO2 vs. FFF Cores Comparison.

Figure 10.16 Distributed Doppler Coefficient: Reference UO2 vs. FFF Cores Comparison.

Figure 10.17 Moderator Temperature Coefficient: Reference UO2 vs. FFF Cores Comparison.

Figure 10.18 Critical Boron Concentration: 100% vs. 150% of Nominal Power Density FFF Cores Comparison.

Figure 10.19 Radial Core Power Peaking: 100% vs. 150% of Nominal Power Density FFF Cores Comparison.

Figure 10.20 Soluble Boron Worth: 100% vs. 150% of Nominal Power Density FFF Cores Comparison.

Figure 10.21 Distributed Doppler Coefficient: 100% vs. 150% of Nominal Power Density FFF Cores Comparison.

Figure 10.22 Moderator Temperature Coefficient: 100% vs. 150% of Nominal Power Density FFF Cores Comparison.

Table 10.10 Batch Average EOC Burnup Summary at Equilibrium (MWd/kg)

	Fresh	Once	Twice
Case 2	169	277	448
Case 3	204	347	418
Case 4	193	350	467
Case 5	252	456	565
Case 6	470	244	576
Case 7	251	495	604

It should be noted once again that the Pu burnup fraction refers to RG Pu from a typical spent LWR fuel. Much higher destroyed Pu fractions are expected for the case of WG Pu (not considered in this work). Furthermore, the core design challenges would be similar to those reported here but perhaps more pronounced for the WG Pu case.

The following observations are made from the obtained results.

- Fertile free annular fuel cores can be designed to achieve the industry standard 18 month fuel cycle with BOC critical boron concentration below 2000ppm required by the water chemistry considerations.

- Distributed Doppler Coefficient of fertile free annular fuel cores is negative but smaller in magnitude by a factor of 2 to 3 than the reference UO_2 core and only about 15 to 30% smaller than the reference solid fuel pellet fertile free core.
- Soluble boron worth of fertile free cores is lower than that of the UO_2 core by a factor of 3 to 7.
- The boron worth reduces with higher Pu loading. Replacement of 10% of annular fuel pins with water holes allows achievement of boron worth comparable to that of the reference solid pellet fertile free core.
- All annular fuel cases have positive MTC at BOC.
- The MTC becomes less positive with the decrease in Pu loading. The lowest Pu inventory case with water holes (Case 7) has positive MTC only during first 30 days of the cycle.
- We estimated that 10% additional reduction of Pu inventory would result in a negative MTC at all times during the cycle. Such an annular fuel operated at slightly lower power density (140% instead of 150% of nominal) would still achieve 450 EFPD fuel cycle length.
- Ten percent reduction in the core power density will also be required to maintain the same thermal margin since the removal of fuel rods would lead to an increase in linear power per fuel rod.
- Radial core power peaking is generally higher than in the reference UO_2 and the solid fertile free fuel cases. A more thorough analysis is required to develop core loading patterns and burnable poison designs that would reduce the core power peaking by utilizing the described earlier design strategies.
- Similar to improvement in MTC, the power peaking is also expected to be reduced if the Pu loading is further lowered. As already mentioned, this will require reduction of operating power density to preserve the fuel cycle length and discharge fuel burnup. Additionally, slightly lower power density (which would be still much higher than nominal PWR power) would permit tolerating the higher power peaking in the annular fuel core.
- All annular fuel cores utilize enriched burnable poisons, which are likely to increase the fuel fabrication costs.
- Displacement of 10% of the fuel pins in assembly results in a 10% increase in the power per fuel pin in the rest of the fuel pins and corresponding degradation of thermal margins. However, we estimate that the thermal margin can still be recovered by a slight reduction in the core inlet coolant temperature. Here, again, the thermal margin can be preserved at the nominal core inlet temperature if the core power density is lowered by 10%.

10.5 Summary and Conclusions

In this chapter, we explored the feasibility of fully Pu loaded fertile free annular fuel core. The unique feature of the annular fuel geometry is that it allows 50% power up-rate of a standard PWR core without degradation or even with some improvement in thermal margins. Such a core loaded with Pu in fertile free fuel matrix is capable of Pu destruction at 50% higher rate than possible in conventional PWR with solid pellet fertile free fuel.

It was shown that as much as 64% of typical reactor grade Pu can be destroyed per pass through the annular fuel fertile free core. The design of such fertile free annular fuel core appears to be considerably more challenging not just in comparison with a standard UO_2 core but also in comparison with full Pu fertile free core with standard solid fuel pellet geometry and standard PWR power density.

The main design difficulty rises from the fact that the annular fuel core requires at least 50% higher initial Pu loading in order to maintain the desired fuel cycle length of 18 months. Such a high Pu loading results in hardening of the neutron spectrum to the extent that the core MTC becomes positive. Additionally, the reactivity worth of control materials is greatly reduced also as a consequence of the neutron spectrum hardening and therefore requires careful burnable poison and core loading pattern designs.

Replacement of about 10% of the annular fuel pins with water holes was found to be a beneficial strategy for improving the MTC and increasing the soluble boron worth. Degradation of the thermal margin as a result of increased power per pin may be compensated by small increase in the core inlet sub-cooling. We concluded that, slightly more moderate power uprate (40% instead of 50%) can assure the negative MTC at the BOC as well as the same thermal margin since 10% linear power increase per fuel pin due to the removal of some of the pins will be compensated for by 10% reduction in the core power density.

The use of Er and Gd burnable poisons will likely require enrichment in their main neutron absorbing isotopes $\text{Er}167$ and $\text{Gd}157$ in order to reduce the poison residual reactivity penalty and to provide the flexibility necessary for designing the core with acceptable power peaking and BOC critical boron concentration. The isotopic enrichment of burnable poisons relies on advanced technologies and is expected to increase the fuel fabrication costs.

The Doppler Coefficient of fertile free annular fuel is negative in all considered cases although, in magnitude, it is only one third to one half that of the standard UO_2 fuel. Use of Er as burnable poison improves the DC by up to 30%. A detailed fast transients evaluation is required to assess whether such value of DC is adequate to assure the reactor safety.

In summary, based on the performed analyses we concluded that annular fertile free Pu loaded high power density core appears to be feasible although it still has relatively high power peaking and potential for slightly positive MTC at BOC. However, we estimate that limiting the power density to 140% of the nominal case would limit the initial Pu loading sufficiently to assure acceptable core power peaking and negative MTC at all times during the cycle.

11. MANUFACTURING VIABILITY

Ed Lahoda, Westinghouse Electric Company, Herbert Feinroth, Gamma Engineering Corporation, Marcelo Salvatore, INVAP, Diego O. Russo, CNEA (Argentine Atomic Energy Commission), Holly Hamilton, AECL (Atomic Energy of Canada, Ltd.)

This chapter investigates viability of manufacturing the proposed thin annular shape fuel. A broad based evaluation of the possible fabrication processes that are relevant to the optimized annular fuel design was performed by Gamma Engineering. Potential fabrication routes and processing technologies that were evaluated included:

- (1) The sintered ring pellet fabrication route, using current punch and die pressing technology, for fabricating green pellets, followed by sintering and grinding.
- (2) Sintered ring pellet fabrication route with slurry extrusion technology for fabricating green pellets followed by drying and then sintering.
- (3) Sintered ring pellet fabrication route with cold iso-static pressing technology for fabricating green ring pellets followed by sintering processing.
- (4) VIPAC fuel element fabrication route with different particle size components of crushed high density sintered UO_2 fuel material.
- (5) VIPAC fuel element fabrication route with different particle size components of spherical high density UO_2 fuel material formed by processes such as sol gel.

The sintered ring pellet with punch and die (option 1) and VIPAC with crushed sintered particles (option 4) fabrication technologies were selected as the most promising candidates for further evaluation, and demonstration at bench scale.

Although slurry extrusion and cold isostatic pressing technologies are commonly used in other (non-radioactive) ceramic manufacturing operations, there is little data and experience in applying these to uranium oxide fuel manufacture. On the contrary, there is extensive data and experience with the pellet punch and die and sintering techniques, and somewhat less, but not insignificant experience with the vibration compaction technique using crushed sintered particles. Hence it was concluded that, for this NERI project, feasibility and actual demonstration could be achieved by concentrating on those processes that already had a substantial data base in “non-annular” uranium oxide fuel forms.

Two separate manufacturing trials were performed to determine the feasibility of using commercial press and sinter techniques to manufacture annular pellets. In the first trial, described in Section 11.2, a specially designed press and die was utilized in an existing operational commercial press at the Westinghouse Columbia, SC fuel plant, and the resulting annular pellets were sintered, centerless ground and measured. In the second trial, performed by INVAP in Argentina, improved die dimensions were used yielding green and sintered pellets with improved dimensional control, and these were loaded into small and large diameter fuel tubes and seal welded at the ends to demonstrate the entire fuel element manufacturing process. Several trials with different pressing parameters were used in an effort to optimize the final sintered shape of the pellet and minimize the need for surface grinding. Results of this work area presented in Section 11.4.

In parallel with this work on the press and sinter technique, a separate effort was performed by AECL at the Chalk River, Ontario laboratories to develop and demonstrate the VIPAC technique. Section 11.4 describes the initial work to fabricate short length annular fuel elements for irradiation testing at MIT. In order to achieve the required heat rating and heat removal conditions in the MIT research reactor, these test specimens were larger in diameter than the reference NERI annular fuel design, although the annular fuel thickness of about 2 mm was maintained. Follow-on work was performed at Chalk River to determine the feasibility of using VIPAC to make longer length fuel elements (up to 48 inches long) using the VIPAC technique. These trials were performed with cladding tubes of the smaller diameter required by the reference 13 x 13 fuel assembly design. Results are reported in Section 11.5.

11.1 Manufacture of Pellets by Press and Sinter at Westinghouse, Columbia, Plant

Annular pellets with dimensions compatible with the 13 x 13 fuel assembly design have been successfully manufactured at the Westinghouse Electric fuel production facility in Columbia. Seventy green pellets were produced. Sixty of these pellets were sintered and thirty pellets were then extensively measured and then ground (on the outer surface) and re-measured. The overall yield for the thirty selected pellets from green pellets to inspected and acceptable pellets was 97% with the loss of one pellet during grinding. There were no technical issues with making or grinding the pellets. Table 11.1 compares the specifications with the final measurements of the 30 pellets measured during this test. The variation of dimensions and the density from target values was due to low die fill during the pelleting operation which led to a lower than target green density.

The pellets were pressed using one station of a 16 station Courtoy R53 UC press (Figure 11.1). This press has some minimal capability of adjusting die fill between pellets to maintain the desired green density. However, due to the change in feed chute configuration between the large annular pellet (die OD of 16.5 mm) and the solid pellets making up remainder of the pellets (die OD of 11 mm), the die did not completely fill. The press was unable to adequately adjust for the one annular pellet station. During normal manufacturing, all stations would contain the same pellet dies and low die fill would not occur. Therefore low green density is not expected to be an issue. Figure 11.1 shows a comparison of the small solid and large annular pellets. Instead of the green density target of 6.2 g/cm³, the green pellets were around 5.757 g/cm³. This led to forming a smaller than desired pellet with a somewhat reduced final density. Although this lower green density would have been expected to yield softer pellets that would have been more susceptible to damage, 100% of the green pellets that were sintered (60) were recovered as sintered pellets (Figure 11.2).

Measurement	Units	Goal	+/-	Actual Unground	+/-	Actual Ground	+/-
OD	mm	14.100	0.020	13.871	0.022	13.781	0.007
Grinding Removal	mm	0.109	0.020			0.0892	0.010
Corrected OD	mm	14.100	0.020	14.202	0.022	14.110	0.007
ID (not ground)	mm	9.900	0.020	9.608	0.019	9.591	0.022
Corrected ID	mm	9.900	0.020	9.837	0.019	9.820	0.022
Web Thickness	mm	2.100	0.020	2.132	0.007	2.095	0.008
Maximum Inner Gap	mm	0.03		0.044		0.056	
Average Maximum Inner Gap	mm	0.03		0.028	0.007	0.039	0.0078
Height	mm	12.700	0.050	10.446	0.452	10.438	0.471
Green Density	gm/cm ³	6.2		5.757			
Sintered Density	% Theoretical	95%		94.29%	0.17%	94.48%	0.24%
Corrected Density	% Theoretical	95%		94.94%	0.17%	95.07%	0.24%
Surface Finish	microns	0.89		0.87	0.12	1.01	0.07
End Face Cracks	<50% annular Thickness			None		None	
Circumferential Length Cracks	<25% Circumferential Length			None		None	
End Capping or End Flaking	None			None		None	

Table 11.1 – Annular Pellet Characteristics Produced in the Columbia Test

Figure 11.1- Large annular pellets being pressed with normal non-annular pellets

Figure 11.2 - Sintered annular pellets by Westinghouse

The effect of the low green pellet density on the final density is shown in Table 11.2. The correction factor for final density amounted to an addition of 0.65% to the measured sintered density which would bring the expected sintered densities up to just over 95% of theoretical UO_2 density.

Parameter	
Actual Green Density	5.757 g/cm ³
Design Green Density	6.2 g/cm ³
Density Correction	0.1458 % final density/0.1 g/cm ³ (green density)
Density Correction	0.65 %
Design Final Density	95%
Chamfer Volume	0.0039 cm ³

Table 11.2 – Effect of Green Density on Sintered Density

Another parameter affected by low green density is the final diameter. The correction factor for the diameter is developed in Table 11.3. Correcting for the ratio of shrinkage factors between the assumed 6.2 g/cm³ and achieved 5.757 g/cm³ gives a diameter correction factor of 1.0239. This correction factor would bring the final ground OD to 14.11 mm which is within 0.01 mm of the design OD. The ID would be brought up to 9.82 mm which is .08 mm under the design of 9.90 mm. In other words, although these preliminary trials led to deviations from the desired green density and dimensions, they also indicated that with proper die design and fill techniques, the required final sintered density and dimensions can be achieved.

Green Density	Shrinkage Factor
6	1.2017
6.1	1.1951
6.2	1.1886
6.3	1.1823
6.45	1.1731
5.757	1.2170
Shrinkage Ratio	0.9767
Correction Factor	1.0239
Corrected Ground OD(mm)	14.110
Corrected ID(mm)	9.820

Table 11.3 – Effect of green density on final pellet diameters

Recognizing that optimizing the shape of the die and press punch would improve the shape and final density of the finished annular fuel pellets, an additional manufacturing development effort was conducted in the fuel manufacturing R&D laboratory at INVAP and at the test fuel fabrication line of the Argentine Atomic Energy Commission. This is described below.

11.2 Manufacture of Annular Fuel Pellets and Fuel Elements in Argentina

This task was aimed at demonstrating the feasibility of manufacturing annular fuel pellets of the appropriate dimensions with a +/- on the dimensions of <.03 mm. It also aimed at demonstrating commercial processes for fuel element loading and end plug welding. The end-plug, welding and fuel rod loading work was done by INVAP, while the pellets were manufactured by the Argentine Atomic Energy Commission (CNEA) under subcontract to INVAP.

11.2.1 End Plug Welding and Preliminary Assembly Tests

During this portion of the work, alumina pellets were fabricated and an annular grinding process developed. In parallel, end plugs were designed and a welding technique developed to provide a high strength weld for the annular fuel tube. The results of the first tests indicated the need to reduce heat during welding and modification of the geometry of the plug at the weld area and of the tube inner machining dimensions. These adjustments were made and satisfactory welds were obtained. The final, acceptable, welding programs utilized the following equipment and welding parameters:

Process: GTAW orbital
Welding Source: AMI mod. 227
Welding Head: AMI mod 15
Shielding gas: Argon
Pre-purge time: 10s
Post purge Time: 20s
Up-slope time: 0
Down-slope time: 3s

LEVEL 1

Time: 43 s
Pulsed arc: on
Rotation mode: Step
Primary speed: 0 rpm
Back speed: 2.6 rpm
Primary current: 45 A
Back current: 20 A
Primary pulse time: 0.12 s
Secondary pulse time: 0.12s

LEVEL 2

Time: 6 s
Pulsed arc: On
Rotation mode: Step
Primary speed: 0 rpm
Back speed: 2.6 rpm
Primary current: 42 A
Back current: 20 A
Primary pulse time: 0.12 s
Secondary pulse time: 0.12s

Figures 11.3 and 11.4 show the top and bottom welded annular rods respectively.

Figure 11.3 Assembled & Welded Top Plug in Annular Fuel Rod

Figure 11.4 Assembled & Welded Bottom Plug in Annular Fuel Rod

11.2.2 Pellet Fabrication and Characterization

The following activities were carried out:

- Uranium oxide powder characterization
- Tests using the modified Westinghouse die
- Tests using INVAP's die
- Pellet fabrication using the new die

INVAP has noted that the conical shape found in the pellets manufactured at the Columbia facility was due to the slight conical shape at the bottom inside of the die. This conicity is there to facilitate pellet release in the high-speed presses at Columbia. By pressing the pellet in the center of the die, INVAP has been able to virtually eliminate the conicity. Removal of green pellets from the non-conical area of the die has been successful for even very low green density pellets ($\sim 5.5 \text{ gm/cm}^3$).

The parameters used during the manufacturing of the lots are detailed in Table 11.4. In order to get to the high performance level evidenced in the last batch of pellets (see Table 11.4), test runs were carried out at various applied pressures. These test runs are summarized in Table 11.5 and in Figures 11.6 and 11.7. Results were then used to select the pressing parameters for use in fabricating the final batch of pellets presented in Table 11.4.

Figure 11.5 – Sintered Pellets from March 5th Run

Table 11.4 INVAP Pellet Pressing Parameters and Summary of Results
(measurement accuracy -1 micron, SD rounded to 2nd decimal place)

Item	Feb 05 Set	Mar 05 Set
Number	32	180
Die and Punch	INVAP	INVAP
Outer Ø (D) mm		
Top	14.65	14.66
Standard Deviation (SD)	0.01	0.01
Centre	14.64	14.66
SD	0.01	0.01
Bottom	14.63	14.66
SD	0.01	0.01
Inner Ø (d) mm		
Top	9.30	9.30
SD	0.01	0.01
Centre	9.29	9.30
SD	0.01	0.01
Bottom	9.29	9.30
SD	0.01	0.01
Height (h) mm		
Average	10.14	10.10
SD	0.03	0.02
Conicity		
(D-d)/2h	0.001	0.0003
Density		
Absolute g/cm ³		
Average	10.62	10.63
SD	0.01	0.01
Relative %TD		
Average	97.0	97.0
SD	0.1	0.1

Powder: UO₂ from Dioxitec Trade Mark
Furnace Temperature and Time: 1650 °C 2 hours
Atmosphere Noxal 10 (Ar-10% H₂)

Table 11.5 – Results of Varying Pellet Press Force on Pellet Characteristics

Pellet No	Top Outer diameter (mm)	Center Outer diameter (mm)	Bottom Outer diameter (mm)	Top Inner diameter (mm)	Bottom Inner diameter (mm)	Height (mm)	Density		Applied Force (KN)
							Absolute (mm)	Relative (%DT)	
1	14,64	14,83	14,65	9,28	9,30	10,105	10,64	97,08	55
2	14,65	14,64	14,66	9,29	9,31	10,525	10,59	96,64	
3	14,65	14,65	14,65	9,30	9,31	10,988	10,59	96,60	
Average	14,65	14,65	14,65	9,29	9,31		10,61	96,77	
Standard Deviation	0,01	0,01	0,01	0,01	0,07		0,03	0,27	
4	14,73	14,72	14,73	9,34	9,34	10,01	10,63	96,95	60
5	14,72	14,72	14,73	9,34	9,34	10,51	10,62	96,87	
6	14,72	14,71	14,73	9,34	9,34	10,95	10,61	96,81	
Average	14,72	14,72	14,73	9,34	9,34		10,62	96,88	
Standard Deviation	0,01	0,01	0,01	0,01	0,01		0,01	0,07	
7	14,79	14,78	14,79	9,37	9,34	10,01	10,63	96,95	65
8	14,79	14,75	14,78	9,36	9,37	10,51	10,62	96,87	
9	14,78	14,77	14,79	9,38	9,38	10,95	10,61	96,81	
Average	14,79	14,77	14,79	9,37	9,36		10,67	97,32	
Standard Deviation	0,01	0,01	0,01	0,01	0,02		0,02	0,10	
10	14,74	14,76	14,83	9,34	9,42	9,92	10,66	97,30	70
11	14,83	14,82	14,84	9,40	9,40	10,23	10,68	97,47	
12	14,83	14,83	14,84	9,41	9,40	10,70	10,69	97,53	
Average	14,80	14,80	14,84	9,39	9,41		10,68	97,43	
Standard Deviation	0,06	0,04	0,01	0,04	0,01		0,02	0,12	
13	14,84	14,88	14,91	9,42	9,42	9,77	10,68	97,45	75
14	14,90	14,89	14,91	9,45	9,46	10,19	10,70	97,59	
15	14,91	14,90	14,91	9,45	9,45	10,56	10,69	97,46	
Average	14,88	14,89	14,91	9,44	9,45		10,69	97,50	
Standard Deviation	0,03	0,01	0,01	0,02	0,02		0,01	0,08

Figure 11.6 Graphical representation of data from Table 11.5. Diameters vs. applied force.

Figure 11.7 Graphical representation of data from Table 11.5. Density vs. applied force

11.2.3 Rod Loading

Three 1-foot rods were fully loaded with pellets, welded and filled with helium gas. Figures 11.8 and 11.9 show the fully assembled fuel pins containing the annular fuel pellets, and with both end plugs welded inside and outside.

Figure 11.8 Finished fully assembled annular rods

Figure 11.9 Fully assembled annular rods – front view

The dimensions of the three rods, as manufactured, are summarized in Table 11.6. The assembly sequence involved first sliding the annular pellets into the outer tube followed by insertion of the inner tube inside the annular pellet rings, then welding the bottom plug

to the outer cladding followed by welding the top plug to the outer cladding and finally welding the bottom and top plugs to the inner cladding. Loading was successfully accomplished with the pellet and cladding tolerances achievable.

Table 11.6 Dimensions of the 3 fully assembled rods manufactured at INVAP

Rod #1	
Cladding	
Outer : Inner Diameter	14.390 mm
Inner : Outer Diameter	9.463 mm
Pellets	
Outer Diameter	14.32 mm
Inner Diameter	9.52 mm
Radial Gap Outer Cladding –Pellets	0.035 mm
Radial Gap Inner Cladding –Pellets	0.035 mm
Rod #2	
Cladding	
Outer : Inner Diameter	14.390 mm
Inner : Outer Diameter :	9.462 mm
Pellets	
Outer Diameter	14.326 mm
Inner Diameter	9.524 mm
Radial Gap Outer Cladding -Pellets	0.032 mm
Radial Gap Inner Cladding –Pellets	0.031 mm
Rod #3	
Cladding	
Outer : Inner Diameter	14.398 mm
Inner : Outer Diameter	9.462 mm
Pellets	
Outer Diameter	14.336 mm
Inner Diameter :	9.523 mm
Radial Gap Outer Cladding –Pellets	0.031 mm
Radial Gap Inner Cladding –Pellets	0.030 mm

11.2.4 Summary of Results of Work in Argentina

The most important results of this work in Argentina are:

1. A batch of pellets (approximately 200) were produced using an INVAP designed punch and die. Annular pellets of the desired size and tolerances were obtained.
2. The conical shape of the annular pellets has been drastically reduced compared to the previous trials and manufacturing tolerances selected and achieved for the subsequent grinding work ($\sim \pm .01$ mm).
3. Statistical dispersion within pellet sizes and tolerances were significantly reduced.
4. Equipment, techniques and procedures for grinding (including the annulus of the pellet) have been demonstrated.
5. End plugs were designed and a welding technique successfully demonstrated.
6. As for manufacturing problems, a failure rate in the traditional sense of broken pellets did not occur, what actually happened is that some pellets developed minor defects on their top during their pressing. Approximately 5% of the pellets of the production series had this type of defect and were therefore discarded. After sintering no failure of any type appeared.

11.3 Manufacture of Annular Fuel Irradiation Test Specimens With VIPAC Technology

AECL completed the fabrication of six annular fuel test specimens and delivered the finished products to MIT in January 2003 for irradiation testing. The test specimens (Figure 11.10) were fabricated utilizing VIPAC fuel processing technology. The primary differences between the VIPAC annular test specimens and the reference annular fuel are in the diameter and the length of the fuel, specifically;

	Test specimens	Reference design
Outer cladding ID	29.75 mm	14.22 mm
Inner cladding OD	25.62 mm	9.78 mm
Fuel length	70.00 mm	3660.0mm

Figure 11.10 – VIPAC Annular Fuel Specimens Being Tested in MITR-II

The cladding wall thickness of the VIPAC test specimens is 1 mm versus the 0.58 mm for the reference annular fuel design. An important similarity between the two fuel designs is the thickness of the fuel annulus (i.e., the gap between the outer cladding ID and the inner cladding OD), which is 2.07 mm for the VIPAC test specimens and 2.22 mm for the reference design. The small difference (i.e., 0.15 mm) is due to the allowance for gaps between the fuel and the cladding surfaces in the pellet configured design.

The processing parameters developed by AECL and the resulting product characteristics that are relevant to the fabrication of the reference annular fuel are summarized in the following:

1. High density (>97% theoretical) crushed 4.91wt% enrichment UO₂ sintered fuel was used as feed material
2. Due to the small annulus gap for fuel material (i.e., ~2 mm), the optimum fuel feedstock was found to be two size components (i.e., ~70 wt% of 0.25-0.50 mm granules and ~30 wt% 0.025-0.053 mm granules) for achieving the highest smear density in the finished product*
3. The maximum smear density of the VIPAC processed fuel (i.e., by vibration packed processing only) achieved by AECL for the annular test specimens was ~77% theoretical. This ~77% theoretical upper limit is consistent with the maximum density reported by other researchers utilizing two component VIPAC technology.
4. AECL developed a mechanical impact processing technique in concert with the VIPAC fuel loading, and increased the fuel smear density to ~82% theoretical in the finished products. The mechanical impact processing involved the insertion of a solid, close-fit metallic cylinder into the fuel annulus after fuel vibration packing, and application of impact forces to the open closed end of the cylinder. This resulted in crushing of the UO₂ fuel granules and thereby increased the smear density.
5. Utilizing a similar fuel loading scheme, AECL fabricated three test specimens with depleted uranium metal mixed in the UO₂ fuel feedstock. The make-up of the fuel was:
 - ~55 wt% of 0.25-0.50 mm sized UO₂ granules,
 - ~15 wt% of 0.25-0.50 mm sized depleted U metal granules, and
 - ~30 wt% 0.025-0.053 mm UO₂ granules.
6. Since the specific density of the depleted U metal is higher than the UO₂ material, the 'apparent' smear density of the products was higher at ~87.5% theoretical.

The idea of adding uranium metal granules to the uranium oxide granules to improve the performance of the VIPAC fuel was adopted from applications in Russia for LWR fuel,

* For a 9mm diameter cylinder, 3 size components would have been used with the third component being of 1-2mm granules.

as reported in Mayorshin et al. [1997]. The development of VIPAC in Russia was originally applied to recycled fast reactor fuel, with the recycled powder produced via an electrochemical process from irradiated spent fuel. The uranium metal addition served at least three functions: it served as an oxygen getter, thereby minimizing fuel clad interactions in high burnup fuel, it increased the thermal conductivity of the fuel mass, thereby lowering the central temperature, and it also increased the total uranium loading allowing higher energy production from a given volume. Mayorshin et al. [1997] reports good performance results of LWR MOX VIPAC fuel having up to 30% Uranium metal content in the MIR test reactor at burnups up to 31,500 MWD/T. It is interesting to note that without the uranium metal addition, the VIPAC fuel produced in the Russian program had a maximum density of 9.4 g/cc, or about 86 % of theoretical density. We speculate that the higher density achieved with vibration compaction in Russia, as compared with that achieved in the annular fuel work reported herein, may be due to the unique powder morphology, and the fact that there is less frictional resistance to compaction in a cylinder than in an annular space.

The completed AECL work has provided valuable insight on the fabrication techniques and product characteristics of annular fuel that is similar to the reference fuel design. The following is a summary of the lessons learned from the AECL work on VIPAC fuel fabrication.

1. By vibration packing processing alone, the achievable maximum fuel smear density of the reference fuel will likely be ~77% with UO_2 granules.
2. The mechanical impact fuel compaction technique developed and utilized by AECL will be difficult to apply in commercial manufacturing operation primarily due to the smaller diameter and much longer reference fuel design.
3. The possibility and advantage of adding uranium metal (depleted or enriched) in the UO_2 fuel feedstock require further analysis and testing. A potential issue is the axial distribution of the added uranium metal in the final fuel product. The density of the uranium metal is 18.9 g/cc and the density of the UO_2 is 10.7 g/cc. Under vibration agitation, the heavier metallic particles could separate and gravitate towards the bottom of the fuel element.

These findings indicate that the VIPAC technology has serious limitations for commercial manufacturing the reference annular fuel at this time. However, the VIPAC fuel fabrication route remains an attractive approach for manufacturing the reference annular fuel due to a number of attributes, including the non-existence of the fuel-to-cladding gaps, thereby mitigating any potential un-even heat split problem. Major advances in this technology may be possible with continued research and development.

11.4 Trial Manufacture of Long VIPAC Annular Fuel Elements

This section describes the results of AECL trial fabrication of 4 foot long annular fuel elements of the reference annular fuel design, using the Vibration Compaction (VIPAC) fabrication route. The purpose of this trial was to determine if the results achieved earlier in fabricating small length larger diameter test specimens for irradiation testing could be maintained or improved with the smaller diameter, and longer length elements that would be required for commercial application. If sufficiently high fuel smear

densities could be achieved, this VIPAC process has the potential to achieve more balanced heat flow to the internal and external coolant channels, thereby minimizing the potential DNB concerns with pellet fuel having unequal pellet clad gaps during operation.

The target minimum smear density for these trials was 85% of UO_2 theoretical density. The actual average density achieved on the two four foot long tubes successfully fabricated in these trials was 77%. A summary of the techniques used and the results obtained follows:

Starting Clad Material - AECL acquired stainless steel tubing of the reference annular fuel design dimensions:

Inner tube ID = 8.62 mm, OD = 9.77 mm.

Outer tube ID = 14.29 mm, OD = 15.39 mm

Gap size = 2.26 mm

A stainless steel plug was machined to friction fit between the bottoms of the tubes. This provided the starting alignment and a solid base for compaction.

UO_2 Granules - Natural UO_2 granules were made by granulation of green pellets into appropriate size ranges followed by sintering and sieving. The sieve size ranges were 25-53 μm and 250-595 μm . Immersion density of sintered granules was 10.57 g/cm³.

“Follower” A stainless steel tube was machined to just fit in the annulus between the two tubes. This tube provided force against the granules during compaction and also kept the tubes aligned. The tube was 30 cm tall. Thin rods were silver soldered to the tube to allow for extraction of the tube. The combination of tube and rods was referred to as the “follower”. A picture of the follower is given in Figure 11.11. A follower was also used in the fabrication of the VIPAC annular fuel irradiation test specimens.

Compaction Frame - A metal frame was erected to keep the tube and the follower vertically aligned during the compaction process. The assembled tube and follower were placed within the I.D. of eyebolts attached to the steel frame. This is shown in Figure 11.11.

Procedure - The assembled tube containing granules and the follower was lifted to prescribed heights and dropped onto a steel plate. The compaction force came from instant deceleration of the granules and the weight of the follower.

Results - Tube 1 was filled using gradually increasing mass fractions to explore the effect of the quantity of granules added on the achievable packing density. Increasing the mass of granules added prior to compaction had a slightly negative effect on the achievable packing density in the mass range explored.

The number of compaction cycles required to achieve the maximum densities increased with the mass of granules added. This is shown graphically in Figure 11.12. It was necessary to use a combination of hard (30 cm drops) and gentle (2 cm drops) compaction cycles in order to maximize the packing density. The final packing density of Tube 1 was 76.9%.

Figure 11.11 Compaction frame, annular tube assembly and follower (held)

Tube 2 was first filled with a small mass of granules (15 g) to try and duplicate results achieved in the 2002 fabrication of irradiation test specimens. The maximum density achieved after over 800 drops from a height of 30 cm plus 16 minutes of dropping from 2 cm was 80.6% of theoretical. Following this the tube was filled in 125 g increments with only light tapping between additions. After the tube was filled to the required height a dropping routine similar to that used on the first, small, mass fraction was applied. The maximum density achieved was 74%.

The compaction technique used to fill tube 3 was changed compared to the first two tubes. Tube three was compacted by holding the tube and hitting the bottom with a metal block. This is the same technique that was used in 2002 for the irradiation test specimens. Unfortunately the follower became jammed in the tube following compaction of the first 60 g granule addition. All attempts to remove the follower failed and the tube had to be destroyed to remove it. The maximum density achieved was 75.3% following 1400 hits. This suggests the compaction techniques were equally effective.

Figure 11.12 - Packing density vs. cumulative compaction cycles for three different mass additions of granules

In general, it was observed that the granules compacted to a certain density (in this case approximately 72%) relatively quickly and after this point small increases in density were only achieved following extensive compaction. In several cases forceful compaction resulted in a decrease in packing density once a plateau had been reached. It appeared that the loss in density could be recovered by gentle compaction.

Summary of Results on Trial Fabrication of Long Annular Fuel Elements -

Compaction techniques, UO_2 granules size fractions and weight ratios, similar to those used in the 2002 irradiation test specimen fabrication campaign to achieve 82% packing densities, were applied to tubes with an annulus that met the reference MIT NERI design. The maximum density achieved for any one-granule size addition was 80.6%. The maximum density achieved for one entire tube was 76.9%. The reason for the lower densities compared to the previous work conducted on annular fuel irradiation test specimens is not clear. The major differences between the previous work and these tests are:

- The size range of the large size fraction of granules (250-593 μm here, 250-500 μm previously)
- The diameters of the tubes,
- The tubing material and
- The outer tube inside surface roughness.

These differences are discussed separately below.

The scope of the work did not allow for an exploration of the effect of the increase in the large granule size range. It is known from other packing studies that, as the largest-size

fraction-diameter-to-gap-size ratio decreases from 7, the packing density decreases. In this work and the previous work a size-range of granules was used (for practical purposes). In the previous work the largest-size fraction-diameter-to-gap-size ratio ranged from 8.2 to 4.1. In this work the ratio ranged from 9.0 to 3.8.

In these tests the tubes have significantly smaller diameters, meaning the wall-surface-area-to-packing-volume ratio is smaller than in the irradiation test specimens. Also, the annulus is slightly larger (2.26 mm compared to 2.06 mm). Both of these factors should favour packing density improvement. There is no previous work on annular-fuel packing to indicate if the reduced diameter could be expected to effect packing density.

The irradiation test specimens used Zircaloy-4 tubes while this work was done with 304 stainless steel. Stainless Steel is harder than Zircaloy but it is difficult to imagine that this is a major factor in packing density.

The surface roughness of the inner tube outer surface was rougher than in the previous work (38 μ -inch here compared to 20 μ -inch for the previous work). Outer tube inside surface roughness values were only slightly higher than the previous work. Improvements in surface roughness were associated with improvements in packing density during the previous work. The extent to which the density suppression seen here is associated with the increased surface roughness cannot be quantified.

Packing densities appear to plateau quickly, after which small gains in density are only achieved following significant effort. This work suggests that packing densities are maximized when granules are added in small increments but the time and effort required to do so may be prohibitive. When granules were added in large increments and then the entire tube packed at once the density was 3% lower than when granules were added in small increments with packing after each addition. Compaction by dropping the assembly or by hitting it on the bottom appeared to produce the same packing results.

The target minimum smear density for these trials was 85% of UO_2 theoretical density. The actual average density achieved on two four-foot long tubes fabricated in these trials was 77%.

In summary, the maximum density achieved for any one-granule size addition was 80.6%. The maximum density achieved for one entire tube was 76.9%. The reason for the lower densities compared to the previous work conducted on annular fuel irradiation test specimens is not clear.

11.5 Application of Zirconia Coating to Mitigate Uneven Heat Split

Analyses of the reference annular fuel design indicated that in some instances, the heat flow from the annular fuel pellets would not be balanced. That is, at the beginning of life, because of differential thermal expansion between the fuel pellets and the zircaloy cladding, there would be too much heat flow towards the outer channel, as compared to the inner channel, causing unacceptable DNB ratios. This imbalance corrects itself after initial burnup, as the fuel pellets swell during irradiation.

One possible step that could mitigate this uneven heat flow early in life would be to apply a thin zirconium oxide partial layer to the outside of the pellets, with sufficient porosity to allow the layer to be compressed after the initial burnup period. In this way, the outer gap conductance would be reduced early in life, allowing more even heat flow in both directions, and then, after fuel swelling begins to fill the inner as well as outer gap, the porous zirconia layer would be compressed, thus again allowing more even heat flow in both directions.

To examine the feasibility of this possible solution, a set of experiments were conducted to determine if such zirconia layers could be easily applied with the required thickness and porosity, and if they might have the desired effect in reducing gap conductance at the beginning of life, and restoring a higher level of gap conductance later in life. A spray coating technique was successfully used to apply coatings of various thicknesses to an alumina substrate (a surrogate for the uranium pellet, and this was pressed with different loads against a zircaloy coupon, and the thermal resistance of the resulting sandwich was measured.

The results indicate that the concept is indeed feasible, that very thin layers of yttria zirconia particles can be applied to the surface of a fuel pellet, that the insulating layer can have the desired insulating properties at the beginning of life, and that increasing contact pressure will increase the conductance later in life. Because only a limited set of parameters could be simulated in these experiments, it was not possible to determine if actual contact pressures predicted to occur during life would be sufficient to restore the required gap conductance later in life. Moreover, later fuel performance and DNBR analyses that used tighter tolerances on the inner gap between cladding and pellets achieved at INVAP manufacturing tests and that accounted for random gap tolerance distribution have shown (see Chapter 14) that the uneven heat flow distribution can be avoided without the need for zirconia partial layers.

11.6 Conclusions

A new high-power density PWR fuel of annular shape, with two claddings, and cooled from both the inside and outside surfaces has been proposed that requires some changes to the current fuel manufacturing processes currently in use. Investigations, including bench scale trials, were conducted to determine if these changes could be handled with current technology. It was found that the current press, sinter and grind technology used in current nuclear fuel plants could be adapted to the annular fuel, and no major changes in fuel manufacturing processes would be required. Achieving dimensional tolerances on the pellet ID surface as may be required to achieve a more balanced heat split remains to be demonstrated.

Based on tests performed at AECL, it appears that annular fuel elements can also be made via the VIPAC manufacturing process, but only to smear densities of 77% as compared to the needed 85%. Addition of elemental uranium powder, as has been demonstrated in some Russian LWR solid fuel tests, can increase the effective smear density to the required 85% or even above [Mayorshin et al., 200]. The effect on fuel performance of an annular fuel element with such a fuel composition is being tested at MIT using VIPAC test specimens prepared by AECL.

12. MANUFACTURING COST AND ECONOMIC BENEFITS

Edward Lahoda and Jason Mazzocchi (Westinghouse Electric Company, LLC)

The objective of the study presented in this chapter was to determine both the manufacturing costs and the end user's economic incentive for utilizing annular fuel. The first task was to identify what manufacturing issues will be encountered when an annular pellet is made and a central tube is introduced in the annulus of the fuel rod. The results of this task became the basis for the second task; estimating the cost difference between the traditional solid and annular fuel pellet manufacturing processes. Estimates were also made of the manufacturing changes that would be required for the use of crushed and bead fuels, though other studies indicated that these fuel forms could not achieve a high enough fuel density to be attractive alternatives to sintered fuel. Finally, the economic benefits due to the use of this annular fuel in uprated current (Generation II) and in new (Generation III) plants were estimated and evaluated using a rate of return on invested capital analysis.

12.1 Costs for Manufacturing Annular Fuel

The first task identified manufacturing issues that will be encountered when a central tube is introduced in the annulus of a fuel rod. These results were the basis for estimating the manufacturing cost differences between traditional solid fuel pins and annular fuel pins.

12.1.1 Description of Manufacturing Process

The current permitting and operational constraints at the Westinghouse Nuclear Fuels plant in Columbia, South Carolina provides the baseline constraints and assumptions used in this evaluation. Figure 12.1 shows a diagram of the manufacturing operations carried out at this plant. These operations are:

1. Receiving – This portion of the plant receives the large containers of UF_6 . These containers are currently stored outdoors until needed.
2. Vaporization – This operation includes the indoor storage and handling of the UF_6 cylinders as well as the subliming of the UF_6 that is performed in large steam chests.
3. Hydrolysis – The UF_6 gas is absorbed in water to form a UO_2F_2 solution.
4. Precipitation – The UO_2F_2 solution is mixed with NH_4OH solution to form ammonium diuranate (ADU) slurry.
5. Cake Recovery – The slurry is centrifuged to separate the ADU from the solution and the recovered cake is pumped to the dryer.
6. Calcination – A two step process where the wet ADU is first dried in a rotary dryer and then calcined to UO_2 using a N_2 , H_2O and H_2 atmosphere in a gas fired calciner.

7. Powder Preparation – The UO_2 powder is milled and blended in preparation for pelleting.
8. Pellet Operations – the UO_2 powder is pressed into pellets and sintered to form 95.5% theoretical density final pellets.
9. Rod Fabrication – The pellets are loaded into rods and the rods assembled into fuel assemblies and shipped.
10. ADU Liquid Waste Processing – The liquid from the Cake Recovery Operation is monitored for low uranium concentration (typically 15 ppm U or less) and then treated in the following steps before being discharged to the Congaree River:
 - Waterglass – residual uranium is precipitated with waterglass and recovered from the waste stream using filtration;
 - Ammonia Recovery – addition of slaked lime and caustic to tie up the fluoride and nitrate, respectively, and distillation to recover ammonia;
 - CaF_2 Recovery – the CaF_2 is allowed to settle in lagoons and the liquid is then discharged to the Congaree River.
11. Uranium solids recovered from waterglass and other scrap operations in the plant are treated in the following steps:
 - Oxidized as required to UO_3 to minimize UO_2 content;
 - Dissolved in nitric acid;
 - Removed from the nitric acid solution using solvent extraction to produce a clean uranium nitrate;
 - The raffinate from the solvent extraction system is neutralized and sent to the CaF_2 lagoons;
 - The extract uranyl nitrate is spiked with HF and sent to the precipitators as recycled uranium.

12.1.2 Manufacturing Changes to Make the Annular Fuel

Several process changes will be required to make annular fuel in the current, solid pellet manufacturing facility due to differences in the product. Annular fuel can be made in at least two ways from UO_2 feed material. The preferred approach uses a sintered, annular, ceramic UO_2 pellet. Few manufacturing changes are required for this approach since both the outer and inner diameter tolerances can be achieved. The alternative method is to use sintered UO_2 particles that are vibrated into place in the fuel rod (vibrapac). The preferred method for vibrapac is to make the sintered UO_2 particles using pressed calcined UO_2 powder (at about 6.5 gm/cm^3) shapes that are broken and sieved to the required size ranges. Another approach would be to use gel-formed, sintered UO_2 particles.

Figure 12.1 Overall schematic of Westinghouse Columbia manufacturing operations

12.1.3 Annular Pellets - Pellet Fabrication

Annular pellets are currently manufactured at the Columbia facility (Table 12.1) with reasonable yields of 85% to 90%. However, the dimensions of the annulus are not as critical as for this application. With this fuel concept, gap requirements on both the inner and outer channels will have to be met to within ± 0.02 mm. Based on extensive testing at both the Columbia plant and by INVAP (see Chapter 11), meeting the tolerance specifications is not expected to be an issue.

Table 12.1 - Comparison of current and proposed annular pellet designs

	Solids Pellet 17x17	Annular Pellet (PQN-02, 13x13)	Largest W Annular Pellet	Smallest W Annular Pellet	
Diameter	9.677	14.100	9.563	7.640	mm
Length	12.700	12.700	12.700	12.700	mm
Annulus ID	NA	9.900	4.648	3.937	mm
Web Thickness	NA	2.100	2.457	1.852	mm

As with any new product, there initially may be additional waste generation due to higher than normal pellet breakage, etc. However, the long-term rates of pellet yields should be about the same as is currently encountered with solid pellets. No cost penalty is accrued during the initial startup period in this study. Tooling changes will be required for the pellet and rod manufacturing operations. In the pellet manufacturing area, these changes include:

- Forming – new dies, feed chutes (assumed to be included as part of normal operational maintenance costs);
- Green Pellet Handling – modified green-pellet vacuum pickups for loading boats (assumed to be a relatively minor cost covered under normal operations expense);
- Grinding – no investment necessary presuming that close to final dimension sintering can be achieved for the interior hole;
- Inspection – no additional investment required.

Changes required in the rod fabrication and assembly fabrication areas include:

- Pellet loading - method of loading pellets into the rod with annular rod. An engineering evaluation indicates the best approach will be to grind the outer diameter to size. The inner hole will be formed and sintered to final size with only a + tolerance allowed. The pellets will be loaded into the outer rod as they are now. Then the inner rod will be inserted. This approach will require only minor changes in the layout of the rod loading operations.
- End plug welding – A new method and additional equipment will be required for welding the end plugs onto an annular rod. The approach that is proposed is as follows. The bottom plug will be welded to the inner annular tube at a new welding station that is converted from a current outside-the-tube welding station. After insertion of the inner tube, the outside lower weld will be completed at a current outside-the-tube welding station. Next, the spring and a new design annular plug will be pressed into the top end of the tube. This annular plug will also contain weep holes to allow for internal rod pressurization with He. This plug will then be welded to the two rods and the weep holes welded over at a modified upper plug welding station. See Figure 12.2 for a diagram of the plugs and location of the various welds.
- Leak testing and rod marking should not require any major changes. The final fabrication area where rods are formed into an assembly will require some new

tooling. However, this tooling cost will be within the normal costs associated with any new product and will therefore not be considered.

Some additional floor space will be required for zirconium rod inventory control. However, this space is relatively insignificant and these costs will not be included as part of the facility upgrade.

The larger pellet diameter of the annular fuel pellets results in lowered capacity in the sintering furnaces. However, 41% fewer pellets are required per assembly and the sintering time has been shown to be reduced by 50% or more. Therefore, although fewer pellets will fit in each boat (49%), more sintering furnaces will not be required for the same production rate (Table 12.2).

Since annular fuel uses a second piece of zirconium tubing in each rod, a second fuel rod loading step for annular pellet fuels is required. As noted above, the number of rods in an annular assembly is about 41% lower than for a solid pellet assembly. Therefore, there should be only a minor change in the number of rod loading lines due to the current overcapacity in fuel rod loading.

Table 12.2 – Comparison of manufacturing capacities for annular and solid pellets

Assumption:

1. Square pitch loading in sintering boats; same residence time
2. Rods have 10 feet of pellets with 20 control rods and thimble tubes per assembly

	Solids Pellet 17x17	Annular Pellet (PQN-02, 13x13)	
Diameter	9.677	14.100	mm
Length	12.700	12.700	mm
Annulus ID	NA	9.900	mm
Web Thickness	NA	2.100	mm
Gross Volume	0.934	1.982	cm ³
Net Volume	NA	1.005	cm ³
Pellet Density	10.467	10.467	gm/cm ³
Pellet Mass	9.77	10.52	gm
Gross Density in Sintering Boat	8.22	4.17	gm/cm ³
Number of Pellets per Assembly	64,560	35,760	pellets
Net Pellet Line Fuel Assembly Capacity Reduction	NA	5%	

Annular Fuel Rod End Plugs and Assembly (Not to Scale)

Figure 12.2 - End plug welds

12.1.4 Annular Particle Fuel – Crushed Particle Manufacture

The second approach uses multiple sized ceramic particles that are vibrated into the rods. The fuel can be formed in either of two ways. The preferred approach uses UO_2 powder that is pressed into any convenient shape at a density of from 6.2 to 6.5 gram/cm^3 . These shapes are then crushed and screened into the appropriate size ranges. The non-conforming sized powder is recycled back to the powder press. The pressed particles are loaded into molybdenum boats and sintered at the usual conditions. During sintering, the

pieces tend to sinter together to form a porous block. After sintering, these blocks are lightly milled and screened to produce free flowing particles. The alternate approach is to use gel-formed bead UO_2 that has been calcined. In either case, the particles are then loaded into rods using vibrators to achieve the maximum final density. The UO_2 powder pressed piece approach is preferred due to the minimal changes required in the current production facility. The gelled bead approach requires changes in the wet conversion and waste treatment sections of the current plant as well as in the rod loading area.

The particles are made in three different size ranges, chosen to provide the densest packing possible after vibratory loading of the fuel rods. The size ranges of the particles that are chosen will not affect the cost of the manufacturing process as a whole. The use of vibrationally loaded powders eliminates major portions of the fabrication area such as pelleting, sintering, grinding and pellet inspection. However, there are new manufacturing issues that must be resolved and their costs determined.

For the crushed powder feed, the following process changes were identified:

- The current presses would be modified to produce pre-forms of some convenient shape. These pre-forms would then be crushed and sieved into the appropriate size fractions in a new crushing and sieving circuit. One would be required for each production line. Machinery for powder recycle back to the press would also be required.
- The current boat loaders would be eliminated.
- Off-spec and contaminated material will be recycled to the oxidation furnaces and thence to the nitric acid dissolution area to be fed to the precipitation columns. Since any excess material can be used in other contracts, there should be a low level of scrap generation. Only about 1 percent of the material should need to be scrapped in this fashion. Due to this low level, these costs are not included in the overall cost estimate.
- Removal of the current pellet grinders and rod loaders.
- Adoption of the current milling stations to break up sintered blocks will be required.
- Current pellet and powder storage areas will be used for particle storage space.
- A new vibratory rod loading station with in-situ density inspection, gas-fill and top end-plug welding will be required. A current outside-the-rod plug welding station will be adopted for the new bottom plug welding station for the internal weld. One of the current outside-the-rod bottom plug welding station can be used for the outside bottom plug weld. Gas fill and end plug welding have to be done at the same station since movement of the rod after it has been filled without a mechanism to hold the powder may allow voids to form. The same top plug design and welding station as was used with the annular pellets will be used for the particle fuel filled rod except that they will be adapted to weld the rod in a vertical rather than horizontal position. The filling rates of the tube will be much lower than the rates for rod loading with pellets. An efficiency reduction of about

75% is assumed. Therefore, at least three more rod loading stations per line would be required.

- Leak testing and rod marking should not require any major changes.
- The final fabrication area where rods are formed into an assembly will require some new tooling. However, this tooling cost will be within the normal costs associated with any new product and will not be considered.

The costs associated with using and handling additional zirconium tubing must also be determined. Some additional floor space will be required for inventory control. However, this space is relatively insignificant and these costs will not be included as part of the facility upgrade.

12.1.5 Annular Particle Fuel – Gel Formed Particle Fuels

For the gel formed particle fuels, the following process changes were identified:

- The current precipitation, centrifuge, and milling equipment will be removed and disposed of.
- Installation of a series of towers to form gel beads, a means of separating the formed beads and moving them to the dryer and calciner is required. High temperature calcination would be desirable to achieve some level of densification so that inter-particle sticking is not encountered in the sintering furnaces. The current calciners that operate at about 600°C would be adapted to operate at 900°C.
- Installation of a new waste treatment system will be required to handle the used organic gellation medium.
- The current pellet presses and boat loaders will be eliminated.
- Off-spec and contaminated material will be recycled to the oxidation furnaces and thence to the nitric acid dissolution area to be fed to the gellation columns. Since any excess beads can be used in other contracts, there should be a low level of scrap generation. Only about 1 percent of the material should need to be scrapped in this fashion. Due to this low level, these costs are not included in the overall cost estimate.
- Since the green beads are sintered together in boats, milling station to break up sintered blocks will be required. The current milling stations would be adapted to this service.
- Pellet and powder storage areas will be used for bead storage space.
- A new vibratory rod loading station with in-situ density inspection, gas-fill and top end-plug welding will be required. A new bottom plug welding station for the internal weld would be made up by adapting a current outside-the tube welding station. The current bottom plug welding station can be used for the outside bottom plug weld. Gas fill and end plug welding have to be done at the same station since movement of the rod after it has been filled without a mechanism to

hold the beads allow voids to form. The same top plug design and welding station as was used with the annular pellets will be used for the particle fuel filled rod except that these would be adapted for use on a vertical rather than horizontal rod. The efficiency loss due to longer loading times is estimated at about 50% for the bead process. Therefore, each line will require one additional rod loading station.

- Leak testing and rod marking should not require any major changes.
- The final fabrication area where rods are formed into an assembly will require some new tooling. However, this tooling cost will be within the normal costs associated with any new product and will not be considered.

The costs associated with using and handling additional zirconium tubing must also be determined. Some additional floor space will be required for inventory control. However, this space is relatively insignificant and these costs will not be included as part of the facility upgrade.

12.1.6 Marginal Fuel Cost

Based on the analysis above, the particle fuel alternatives will not be used because of their more extensive capital charges and because of the much lower smeared density that they can obtain. The lower smeared density is especially important to consider since the annular fuel concept is fuel short for the higher power densities that it can deliver. As was noted in the sections above, the manufacture of sintered annular pellets will result in few manufacturing changes, none of which are of significant cost. The only additional cost of any significance is for the additional zirconium metal. Therefore, these zirconium costs will be used to obtain the marginal cost increase of the annular fuel. Based on the analysis in Table 12.3, the cost of fuel increases from the current \$0.005/kWhr(e) [Nuclear Fuel, 2002] to about \$0.00502/kWhr(e).

Table 12.3 - Marginal costs for annular fuel

Burnup		66 MWd/kg U
Burnup		1,584,000 KWhr/kg U
Burnup		554,400 KWhr(e)/kg U @ 35% Efficiency
Marginal Cost Due to Zirconium		
Current Total Zr/U Weight Ratio		0.200 kg Zr/Kg/U
Annular Total Zr/U Weight Ratio		0.339 kg Zr/Kg/U
Fabricated Zr Tube Cost	\$	66 /kg Zr
Marginal Zr Cost	\$	9.14 /kg U
Marginal Power Cost	\$	0.000016 /KWhr(e)
Total Marginal Cost Increase	\$	0.00002 /KWhr(e)

12.2 Economic Impact of Annular Fuel

The economic impact of using annular fuel as an upgrade in current reactors and as a means to upgrade the power density in future reactors was determined. Both fuel cycle costs and power plant equipment costs have been considered. One issue that was assumed to be addressed by changes in the fuel configuration was that of the fuel/power density. Annular fuel has a higher usage rate in the reactor due to its higher power level per volume. Since the overall uranium content of the core is the same, this results in a shorter cycle life unless more ^{235}U is added to the core. For instance, if power levels are increased by 50%, current 18 month cycles would decrease to about 12 to 14 months. Several alternatives are available. The most straightforward is to use a ^{235}U enrichment of 8% versus the current 5%. However, this change impacts all the wet process areas of the plant and is the least desirable approach and will not be evaluated. Another approach is to increase the volume of UO_2 in the rod. This can be done by increasing the fuel rod length from 12 to 14 feet and by increasing the density from 95.5 to 97% of theoretical density. This combination increases the ^{235}U content by about 18%. These changes along with an increase in the fraction of fuel replaced in each 18 month outage should allow maintenance of close to 18 month cycles. Finally, the use of higher density fuel materials such as uranium carbide or uranium nitride, and the use of beryllium (Be) reflectors can be used though these options will require more extensive changes in the fuel and the reactor and require significant license changes.

12.2.1 Options Considered

Five options are evaluated. These options are summarized in Table 12.4 below. The fuel and operations and maintenance cost and benefit assumptions are as follows:

1. Baseline fuel costs were assumed to be \$.005/kWhr [Nuclear Fuel, 2002]. This is the average cost used for fuel in current Generation II PWRs. The marginal costs for annular fuel are provided below.
2. Operations and Maintenance (O&M) costs of \$.01/kWhr(e) were assumed for Generation II and Generation III plants using pellet fuel. For uprates due to the use of annular fuel, the additional O&M costs on the increased power output was assumed to be \$.005/kWhr(e). We assumed that only the new balance of plant (BOP) would require additional O&M costs. The O&M costs on the existing nuclear island (NI) were assumed to cover all the costs for the uprated power level. For a new Generation III plant using annular fuel, an O&M cost of \$.0066/kWhr(e) was used. This number was arrived at by assuming that the original \$.01/kWhr(e) on the original 1,000 MW(e) covers all the O&M costs for the new plant. The effective new cost is therefore $\$.01 * 1,000 / 1,500$ or \$.0066/kWhr(e).

The details of these options summarized in Table 12.4 are as follows:

1. Option 1 is a new construction Generation III PWR running at 1,117 MW(e). A 3-year construction time is assumed. The cost of this option is \$1,311 [Paulson 2002]. A model was made for the capital cost for Generation III power plants based on work performed by Westinghouse and EPRI for the contract [Lahoda et al., 2005]. This model calculates an installed capital cost of \$1,313/KW(e).

2. Option 2 is a currently operating Generation II PWR (for instance, a 1,200 MW(e), four loop Westinghouse PWR) uprated to 1,800 MW(e). This option was studied as part of the NEPO funded program [Westinghouse, 2005]. This uprate is accomplished by replacing the core internals to accommodate the annular fuel. In addition, the old recirculation pumps are replaced with uprated models if the uprate is greater than 25%, the four current square pitch steam generators replaced with four triangular pitch steam generators [Hu, 2002] and the pressurizer is replaced to increase capacity. The objective is to fit the higher power components of the nuclear island (NI) (reactor, pumps, pressurizer and steam generators) within the 1,000 MW(e) sized containment. Most balance of plant (BOP) components in operating Generation II PWRs are already at capacity and cannot be readily uprated [Liu, 2002 and McFetridge, 2002]. Therefore, it is assumed that an additional BOP rated at 600 MW(e) is built in parallel with the existing BOP. The construction of the BOP, replacement steam generators, circulation pumps and reactor internals proceeds while the plant is in operation. During a 12-month shutdown, the steam generators, circulation pumps and reactor internals are replaced and the BOP connected to the main steam line from the NI. This option includes the costs associated with the installed equipment, un-utilized fuel value and replacement power (\$1,817/KW(e)).
3. Option 3 is a new construction Generation III PWR uprated to a capacity of 1,717 MW(e). This uprate is accomplished by substituting: enhanced core internals to accommodate the annular fuel including a two-foot extension to the fuel and core reflectors; uprated recirculation pumps and pressurizer; and four triangular pitch steam generators for four square pitch steam generators. The objective is to fit the higher power components of the NI (reactor, pumps and steam generators) within a containment that is smaller than one that would result from using standard fuel. A 1,717 MW(e) BOP is used instead of a 1,117 MW(e) BOP. A 3-year construction time is assumed. In order to estimate the capital cost for this option, the following adjustments were made to the cost model based on standard fuel developed for Option 1. This model is:

$$\text{Installed Capital Cost (\$/KW(e))} = 10^{(4.06-0.309*\text{LOG}(\text{Power (MW(e))}))}$$

The adjustments are for a smaller containment (assumed to be 10% smaller) and reactor vessel and internals (assumed to be 25% smaller) than for the standard fuel versions. The cost models for these two components are:

Reduction in Installed Containment Cost

$$(\$/\text{KW(e)}) = 0.1 * 10^{(-0.01-0.6598*\text{LOG}(\text{Power (MW(e))}))}$$

$$\text{Installed Reactor Vessel Cost } (\$/\text{KW(e)}) = 0.75 * 10^{(-1.1-0.9992*\text{LOG}(\text{Power (MW(e))}))}$$

The net installed capital cost for this option is therefore \$1,103/KW(e).

4. Option 4 is a new construction Generation III PWR uprated to 1,717 MW(e) using the current design pellet fuels. In this case, the containment and all other components are scaled to the 1,717 MW(e) capacity. A 3-year construction time is assumed. The cost model is the same as the base case in Option 1 above or \$1,150/KW(e).
5. Option 5 is a new construction Generation III PWR uprated to 1,117 MW(e) using the annular design pellet fuels. In this case, the containment and all other components are

scaled to the 1,117 MW(e) capacity and adjusted as indicated in Option 3 above for the smaller containment and reactor vessel. A 3-year construction time is assumed. The estimated cost for this Option 3 is \$1,281/KW(e).

Table 12.4 – Summary of Options

Option	Description	Size (MW(e))	Fuel Type	Construction Time (years)	Fuel Cost (\$/kWhr(e))	O&M Cost (\$/kWhr(e))	Capital Cost (\$kW(e))
1	New Construction, Generation III	1,117	Solid	3	\$0.01	\$0.01	\$1,313
2	Uprate Generation II from 1,000 MW(e)	600 added to 1,200 initial Total 1,800	Annular	3 years for component construction Plant offline 12 months	\$0.00502 on all 1,800 MW(e)	\$0.005 on added 600 MW(e)	\$1,817 on added 600 MW(e)
3	New Construction, Generation III	1,717	Annular	3	\$0.01	\$0.01	\$1,103
4	New Construction, Generation III	1,717	Solid	3	\$0.01	\$0.01	\$1,150
5	New Construction, Generation III	1,117	Annular	3	\$0.01	\$0.01	\$1,281

The above costs and credits were then used in a rate of return (ROR) on invested capital calculation using the financial parameters shown in Table 12.5. These calculations are summarized in below.

Table 12.5 - Financial assumptions for comparison of annular and pellet PWR fuels

Price of Electricity	\$0.05 /kWhr(e)
Discount Rate	11% /yr
Inflation Rate for Power Cost	1% /yr
Inflation Rate for Fuel and O&M Costs	2% /yr
Operating Rate	95% /yr
Operating Time	20 years
Fuel Enrichment	5% 235
Burnup	67 MWd/kg U
Burnup	1,608,000 kWhr/kg U
Burnup	562,800 kWhr(e)/kg U
Plant Efficiency	35%

12.2.2 Calculation of Annular Fuel Benefit

Discounted cash flow calculations were performed for each of the options listed above. The results are shown in Tables 12.6 to 12.10. The discounted rates of returns for the various options were (listed best to worst):

1. 1,717 MW(e) Generation III new PWR; annular fuel (Option 3) – 11.3%
2. 1,717 MW(e) Generation III new PWR; standard fuel (Option 4) – 10.8%
3. 1,117 MW(e) Generation III new PWR; annular fuel (Option 5) – 7.3%
4. 1,117 MW(e) Generation III new PWR; standard fuel (Option 1) – 6.9%
5. 600 MW(e) uprate to a Generation II PWR; annular fuel (Option 2) – 6.3%

The rates of return for the annular fuel options for new reactors are always higher than for reactor options using standard fuel. A comparison of Option 1 and Option 3 shows that getting 50% more power out of the same size nuclear island increases the ROR from 6.9% to 11.3%. A comparison of Options 3 with 5 and Options 4 with 1 shows that increasing the size of the nuclear island to get additional power increases the ROR more than changing the fuel form from standard to annular. The conclusion is that the use of annular fuel to maximize power output from a smaller nuclear island does increase the ROR for any given size plant.

Finally, the 6.3% ROR for upgrading an existing Generation II PWR by using annular fuel indicates that the profitability of existing plants can indeed be improved. However, the ROR is less than for the new reactor options. This is mainly due to the high cost of the replacement power and the unused fuel value during the transition from standard to annular fuel. For instance, if these two items were not included, the capital cost would decrease to \$1,381/KW(e) from \$1,817/KW(e) and the value of this option would increase significantly with a ROR of about 11.6% (Table 12.11). Therefore, if low cost replacement power is readily available during the transition period and if the unused fuel cost can be reduced due to tighter fuel management before the transition, then the uprate option becomes the most favorable of all options.

Table 12.6 - Rate of return for a 1,117 MW(e) Generation III PWR; standard fuel
(Option 1)

Discount Rate		11% /yr			
Inflation Rate for Power Cost		1% /yr			
Inflation Rate for Fuel and O&M Costs		2% /yr			
Operating Rate		95% /yr			
Net Output Power		1117 MW(e)			
Installed Cost Power Plant		\$1,313 /KW(e)			
Total Capital Cost		\$1,466,853,397			
Construction Time		3 years			
Initial Price for Produced Power	\$	0.0500 /KW(e)			
O&M Costs	\$	0.0100 /KW(e)			
Fuel Costs	\$	0.00500 /KW(e)			
Discounted Return on Investment		6.9%			

Year		Costs	Income	Discounted Net Profit	Return on Investment
1	\$	488,951,132	\$ -	\$ (488,951,132)	
2	\$	488,951,132	\$ -	\$ (440,496,516)	
3	\$	488,951,132	\$ -	\$ (396,843,708)	
4	\$	147,969,654	\$ 478,867,111	\$ 241,949,368	-61.9%
5	\$	150,929,047	\$ 483,655,782	\$ 219,177,406	-35.0%
6	\$	153,947,628	\$ 488,492,340	\$ 198,536,003	-20.7%
7	\$	157,026,581	\$ 493,377,263	\$ 179,826,810	-12.3%
8	\$	160,167,112	\$ 498,311,036	\$ 162,869,865	-6.9%
9	\$	163,370,455	\$ 503,294,146	\$ 147,501,896	-3.3%
10	\$	166,637,864	\$ 508,327,088	\$ 133,574,782	-0.7%
11	\$	169,970,621	\$ 513,410,359	\$ 120,954,145	1.1%
12	\$	173,370,033	\$ 518,544,462	\$ 109,518,087	2.5%
13	\$	176,837,434	\$ 523,729,907	\$ 99,156,030	3.5%
14	\$	180,374,183	\$ 528,967,206	\$ 89,767,673	4.3%
15	\$	183,981,666	\$ 534,256,878	\$ 81,262,036	4.9%
16	\$	187,661,300	\$ 539,599,447	\$ 73,556,602	5.4%
17	\$	191,414,526	\$ 544,995,441	\$ 66,576,530	5.8%
18	\$	195,242,816	\$ 550,445,396	\$ 60,253,943	6.1%
19	\$	199,147,673	\$ 555,949,849	\$ 54,527,285	6.3%
20	\$	203,130,626	\$ 561,509,348	\$ 49,340,736	6.5%
21	\$	207,193,239	\$ 567,124,441	\$ 44,643,673	6.7%
22	\$	211,337,103	\$ 572,795,686	\$ 40,390,198	6.8%
23	\$	215,563,845	\$ 578,523,643	\$ 36,538,691	6.9%

Table 12.7 - Rate of return for a 600 MW(e) uprate to a Generation II PWR; annular fuel
(Option 2)

Discount Rate	11% /yr			
Inflation Rate for Power Cost	1% /yr			
Inflation Rate for Fuel and O&M Costs	2% /yr			
Operating Rate	95% /yr			
Increase in Power	600 MW(e)			
Cost of Marginal Power Increase	\$1,817 /KW(e)			
Capital Cost of 500 MW(e) Increase	\$1,090,200,000			
Modification Time	1.00 years			
Initial Price for Produced Power	\$	0.0500	/KWhr(e)	
O&M Costs (BOP only)	\$	0.0050	/KWhr(e)	
Fuel Cost	\$	0.00502	/KWhr(e)	\$ -
Discounted Return on Investment	6.3%			

Year	Marginal Costs	Marginal Income	Discounted Net Profit	Return on Investment
1	\$ 1,115,215,932	\$ -	\$ (1,115,215,932)	
2	\$ 51,032,501	\$ 252,156,600	\$ 181,192,882	-83.8%
3	\$ 52,053,151	\$ 254,678,166	\$ 164,455,007	-52.6%
4	\$ 53,094,214	\$ 257,224,948	\$ 149,258,633	-32.5%
5	\$ 54,156,099	\$ 259,797,197	\$ 135,462,161	-20.3%
6	\$ 55,239,221	\$ 262,395,169	\$ 122,936,973	-12.5%
7	\$ 56,344,005	\$ 265,019,121	\$ 111,566,238	-7.4%
8	\$ 57,470,885	\$ 267,669,312	\$ 101,243,840	-3.8%
9	\$ 58,620,303	\$ 270,346,005	\$ 91,873,392	-1.3%
10	\$ 59,792,709	\$ 273,049,465	\$ 83,367,349	0.5%
11	\$ 60,988,563	\$ 275,779,960	\$ 75,646,196	1.9%
12	\$ 62,208,334	\$ 278,537,759	\$ 68,637,717	2.9%
13	\$ 63,452,501	\$ 281,323,137	\$ 62,276,322	3.7%
14	\$ 64,721,551	\$ 284,136,368	\$ 56,502,443	4.4%
15	\$ 66,015,982	\$ 286,977,732	\$ 51,261,982	4.8%
16	\$ 67,336,302	\$ 289,847,509	\$ 46,505,810	5.2%
17	\$ 68,683,028	\$ 292,745,984	\$ 42,189,308	5.5%
18	\$ 70,056,688	\$ 295,673,444	\$ 38,271,961	5.8%
19	\$ 71,457,822	\$ 298,630,179	\$ 34,716,974	6.0%
20	\$ 72,886,978	\$ 301,616,481	\$ 31,490,937	6.2%
21	\$ 74,344,718	\$ 304,632,645	\$ 28,563,511	6.3%
22	\$ 75,831,612	\$ 307,678,972	\$ 25,907,148	6.4%

Table 12.8 - Rate of return for a new 1,717 MW(e) Generation III PWR; annular fuel
(Option 3)

Discount Rate	11% /yr				
Inflation Rate for Power Cost	1% /yr				
Inflation Rate for Fuel and O&M Costs	2% /yr				
Operating Rate	95% /yr				
Net Output Power	1,717 MW(e)				
Installed Cost Power Plant	\$1,103 /KW(e)				
Total Capital Cost	\$1,893,299,218				
Construction Time Time	3 years				
Initial Price of Produced Power	\$ 0.0500 /KWhr(e)				
O&M Costs	\$ 0.0066 /KWhr(e)				
Fuel Costs	\$ 0.00502 /KWhr(e)				
Discounted Return on Investment	11.5%				

Year		Costs	Income	Discounted Net Profit	Return on Investment
1	\$	631,099,739	\$ -	\$ (631,099,739)	
2	\$	631,099,739	\$ -	\$ (568,558,324)	
3	\$	631,099,739	\$ -	\$ (512,214,706)	
4	\$	176,173,709	\$ 736,092,059	\$ 409,407,472	
5	\$	179,697,183	\$ 743,452,979	\$ 371,363,405	-27.1%
6	\$	183,291,127	\$ 750,887,509	\$ 336,840,827	-13.2%
7	\$	186,956,949	\$ 758,396,384	\$ 305,514,857	-5.2%
8	\$	190,696,088	\$ 765,980,348	\$ 277,090,502	-0.2%
9	\$	194,510,010	\$ 773,640,151	\$ 251,299,913	3.1%
10	\$	198,400,210	\$ 781,376,553	\$ 227,899,894	5.4%
11	\$	202,368,214	\$ 789,190,318	\$ 206,669,637	7.0%
12	\$	206,415,579	\$ 797,082,222	\$ 187,408,670	8.1%
13	\$	210,543,890	\$ 805,053,044	\$ 169,934,986	9.0%
14	\$	214,754,768	\$ 813,103,574	\$ 154,083,347	9.6%
15	\$	219,049,863	\$ 821,234,610	\$ 139,703,745	10.1%
16	\$	223,430,861	\$ 829,446,956	\$ 126,659,998	10.5%
17	\$	227,899,478	\$ 837,741,426	\$ 114,828,485	10.8%
18	\$	232,457,467	\$ 846,118,840	\$ 104,096,984	11.0%
19	\$	237,106,617	\$ 854,580,028	\$ 94,363,631	11.2%
20	\$	241,848,749	\$ 863,125,829	\$ 85,535,960	11.3%
21	\$	246,685,724	\$ 871,757,087	\$ 77,530,043	11.4%
22	\$	251,619,438	\$ 880,474,658	\$ 70,269,703	11.5%
23	\$	256,651,827	\$ 889,279,404	\$ 63,685,797	11.5%

Table 12.9 - Rate of return for a new 1,717 MW(e) Generation III PWR; standard fuel (Option 4)

Discount Rate	11% /yr
Inflation Rate for Power Cost	1% /yr
Inflation Rate for Fuel and O&M Costs	2% /yr
Operating Rate	95% /yr
Net Output Power	1,717 MW(e)
Installed Cost Power Plant	\$1,150 /KW(e)
Total Capital Cost	\$1,974,398,297
Construction Time	3 years
Initial Price of Produced Power	\$ 0.0500 /KWhr(e)
O&M Costs	\$ 0.0066 /KWhr(e)
Fuel Costs	\$ 0.00500 /KWhr(e)
Discounted Return on Investment	10.8%

Year	Costs	Income	Discounted Net Profit	Return on Investment
1	\$ 658,132,766	\$ -	\$ (658,132,766)	
2	\$ 658,132,766	\$ -	\$ (592,912,402)	
3	\$ 658,132,766	\$ -	\$ (534,155,317)	
4	\$ 175,896,222	\$ 736,092,059	\$ 409,610,368	
5	\$ 179,414,146	\$ 743,452,979	\$ 371,549,850	-28.4%
6	\$ 183,002,429	\$ 750,887,509	\$ 337,012,155	-14.4%
7	\$ 186,662,478	\$ 758,396,384	\$ 305,672,294	-6.3%
8	\$ 190,395,727	\$ 765,980,348	\$ 277,235,174	-1.2%
9	\$ 194,203,642	\$ 773,640,151	\$ 251,432,854	2.1%
10	\$ 198,087,715	\$ 781,376,553	\$ 228,022,056	4.4%
11	\$ 202,049,469	\$ 789,190,318	\$ 206,781,894	6.1%
12	\$ 206,090,458	\$ 797,082,222	\$ 187,511,825	7.3%
13	\$ 210,212,268	\$ 805,053,044	\$ 170,029,777	8.1%
14	\$ 214,416,513	\$ 813,103,574	\$ 154,170,453	8.8%
15	\$ 218,704,843	\$ 821,234,610	\$ 139,783,788	9.3%
16	\$ 223,078,940	\$ 829,446,956	\$ 126,733,551	9.7%
17	\$ 227,540,519	\$ 837,741,426	\$ 114,896,074	10.0%
18	\$ 232,091,329	\$ 846,118,840	\$ 104,159,093	10.2%
19	\$ 236,733,156	\$ 854,580,028	\$ 94,420,704	10.4%
20	\$ 241,467,819	\$ 863,125,829	\$ 85,588,406	10.5%
21	\$ 246,297,175	\$ 871,757,087	\$ 77,578,237	10.6%
22	\$ 251,223,119	\$ 880,474,658	\$ 70,313,988	10.7%
23	\$ 256,247,581	\$ 889,279,404	\$ 63,726,492	10.8%

Table 12.10 - Rate of return for a 1,117 MW(e) Generation III PWR; annular fuel
(Option 5)

Discount Rate	11% /yr
Inflation Rate for Power Cost	1% /yr
Inflation Rate for Fuel and O&M Costs	2% /yr
Operating Rate	95% /yr
Net Output Power	1,117 MW(e)
Installed Cost Power Plant	\$1,281 /KW(e)
Total Capital Cost	\$1,430,999,490
Construction Time Time	3 years
Initial Price of Produced Power	\$ 0.0500 /KWhr(e)
O&M Costs	\$ 0.0100 /KWhr(e)
Fuel Costs	\$ 0.00502 /KWhr(e)
Discounted Return on Investment	7.3%

Year	Costs	Income	Discounted Net Profit	Return on Investment
1	\$ 476,999,830	\$ -	\$ (476,999,830)	
2	\$ 476,999,830	\$ -	\$ (429,729,577)	
3	\$ 476,999,830	\$ -	\$ (387,143,763)	
4	\$ 148,166,947	\$ 478,867,111	\$ 241,805,110	
5	\$ 151,130,286	\$ 483,655,782	\$ 219,044,844	-34.3%
6	\$ 154,152,892	\$ 488,492,340	\$ 198,414,189	-20.1%
7	\$ 157,235,950	\$ 493,377,263	\$ 179,714,873	-11.7%
8	\$ 160,380,669	\$ 498,311,036	\$ 162,767,004	-6.3%
9	\$ 163,588,282	\$ 503,294,146	\$ 147,407,375	-2.7%
10	\$ 166,860,048	\$ 508,327,088	\$ 133,487,925	-0.2%
11	\$ 170,197,249	\$ 513,410,359	\$ 120,874,330	1.6%
12	\$ 173,601,194	\$ 518,544,462	\$ 109,444,743	3.0%
13	\$ 177,073,217	\$ 523,729,907	\$ 99,088,634	4.0%
14	\$ 180,614,682	\$ 528,967,206	\$ 89,705,741	4.8%
15	\$ 184,226,975	\$ 534,256,878	\$ 81,205,126	5.4%
16	\$ 187,911,515	\$ 539,599,447	\$ 73,504,306	5.8%
17	\$ 191,669,745	\$ 544,995,441	\$ 66,528,474	6.2%
18	\$ 195,503,140	\$ 550,445,396	\$ 60,209,783	6.5%
19	\$ 199,413,203	\$ 555,949,849	\$ 54,486,706	6.7%
20	\$ 203,401,467	\$ 561,509,348	\$ 49,303,447	6.9%
21	\$ 207,469,496	\$ 567,124,441	\$ 44,609,408	7.1%
22	\$ 211,618,886	\$ 572,795,686	\$ 40,358,711	7.2%
23	\$ 215,851,264	\$ 578,523,643	\$ 36,509,757	7.3%

Table 12.11 - Rate of return for a 600 MW(e) uprate to a Generation II PWR; annular fuel (Option 2) without charges for replacement power and unused fuel

Discount Rate	11% /yr				
Inflation Rate for Power Cost	1% /yr				
Inflation Rate for Fuel and O&M Costs	2% /yr				
Operating Rate	95% /yr				
Increase in Power	600 MW(e)				
Cost of Marginal Power Increase	\$1,381 /KW(e)				
Capital Cost of 500 MW(e) Increase	\$828,600,000				
Modification Time	1.00 years				
Initial Price for Produced Power	\$	0.0500 /KWhr(e)			
O&M Costs (BOP only)	\$	0.0050 /KWhr(e)			
Fuel Cost	\$	0.00502 /KWhr(e)			
Discounted Return on Investment	11.6%				
Year		Marginal Costs	Marginal Income	Discounted Net Profit	Return on Investment
1	\$	853,615,932	\$ -	\$ (853,615,932)	
2	\$	51,032,501	\$ 252,156,600	\$ 181,192,882	-78.8%
3	\$	52,053,151	\$ 254,678,166	\$ 164,455,007	-44.2%
4	\$	53,094,214	\$ 257,224,948	\$ 149,258,633	-23.6%
5	\$	54,156,099	\$ 259,797,197	\$ 135,462,161	-11.6%
6	\$	55,239,221	\$ 262,395,169	\$ 122,936,973	-4.3%
7	\$	56,344,005	\$ 265,019,121	\$ 111,566,238	0.4%
8	\$	57,470,885	\$ 267,669,312	\$ 101,243,840	3.6%
9	\$	58,620,303	\$ 270,346,005	\$ 91,873,392	5.7%
10	\$	59,792,709	\$ 273,049,465	\$ 83,367,349	7.3%
11	\$	60,988,563	\$ 275,779,960	\$ 75,646,196	8.4%
12	\$	62,208,334	\$ 278,537,759	\$ 68,637,717	9.2%
13	\$	63,452,501	\$ 281,323,137	\$ 62,276,322	9.8%
14	\$	64,721,551	\$ 284,136,368	\$ 56,502,443	10.3%
15	\$	66,015,982	\$ 286,977,732	\$ 51,261,982	10.7%
16	\$	67,336,302	\$ 289,847,509	\$ 46,505,810	10.9%
17	\$	68,683,028	\$ 292,745,984	\$ 42,189,308	11.2%
18	\$	70,056,688	\$ 295,673,444	\$ 38,271,961	11.3%
19	\$	71,457,822	\$ 298,630,179	\$ 34,716,974	11.5%
20	\$	72,886,978	\$ 301,616,481	\$ 31,490,937	11.6%
21	\$	74,344,718	\$ 304,632,645	\$ 28,563,511	11.6%
22	\$	75,831,612	\$ 307,678,972	\$ 25,907,148	11.7%

12.3 Conclusions

This study showed that the use of annular fuel to uprate the power levels of both current generation and next generation PWRs increases the rate of return on invested capital. This result is not surprising since a 50% increase in power can be obtained for a very minor increase in fuel costs. In the case of a new Generation III power plant, the use of annular fuel can be used to either get more capacity from the same sized nuclear island or to reduce the size of the nuclear island. For the currently operating Generation II power plants, annular fuel provides a means to significantly upgrade the power output (up to 50%) using the same nuclear island with new reactor internals, steam generators and circulation pumps, and a new incremental balance of plant (built during plant operation) to handle the increased power production. However, the ROR is less than for the new reactor options. This is mainly due to the high cost of the replacement power and the unused fuel value during the transition from standard to annular fuel.

There are some caveats on this conclusion. First, the cost of upgrading systems outside the power plant boundary such as the local power grids was not included. Also not included were major costs associated with site licensing and transition fuel costs (as the current core is replaced with an annular fuel core). Also, an outage of 12 months was assumed. Some estimates as high as 18 months were obtained. These were not used since one would expect that significant effort would be made to minimize the outage length. Finally, it was assumed that changes to the fuel could be made that would provide the added uranium necessary to obtain 50% more power from fuel rods while maintaining the current fuel cycle length (~18 months).

13. THERMO-MECHANICAL PERFORMANCE

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In order to have adequate confidence in the proposed design of the internally and externally (I&E) cooled annular fuel, it is important to identify the fuel operational constraints from a materials performance perspective and evaluate if the new fuel remains within these constraints during irradiation. This chapter describes the models developed for thermo-mechanical analysis of the I&E cooled annular fuel and the fuel performance analysis results. Both the sintered pellet and VIPAC fuel will be evaluated.

13.1 Introduction and Background

The internally and externally cooled annular fuel concept exhibits significantly lower fuel temperature than solid fuel, hence it is expected that fission gas release will be smaller allowing higher burnups. Experience with annular UO_2 fuel pellets is very limited. Sintered annular pellets with central voids (10% by volume) have been manufactured for some Russian VVER reactors and evaluated for PWRs [Mildrum, 1980] to lower their fuel temperature. However, the temperature reduction in these pellets is very small compared to I&E cooled annular fuel and its behavior very different. Annular pellets with natural uranium oxide in the central region were also experimented with [Jensen, 1980] and it was found that the radial expansion of the fuel was reduced, resulting in low mechanical interaction between the fuel and cladding. An annular fuel design with internal and external cooling was proposed in the advanced plutonium fuel assembly (APA) concept in France [Brochard et al., 2001]. The $\text{PuO}_2\text{-CeO}_2$ annular fuel was fabricated with concentric claddings to allow cooling at both the inner and outer sides. Analysis showed significant reduction of fuel temperature and small bulking risk. However, the asymmetry of fuel-clad gaps and fabrication of fuel pellets turned out to be restrictive [Puill, 2001]. Vibration Packing (VIPAC) of fuel is an alternative fuel manufacturing technique for annular fuel design. The VIPAC I&E cooled annular fuel has the same dimensions as the sintered annular fuel except that fuel-cladding gaps are eliminated. The VIPAC fuel was first developed at Pacific Northwest Laboratory in 1959 [Freshley and Bruley, 1969] and MOX fuel rods were manufactured for the Experimental Boiling Water Reactor (EBWR) and Plutonium Recycle Test Reactor (PRTR) [Anderson, 1963]. The fuel particles were pre-pressed with high-energy pneumatic impact and then vibratory compacted into the tube. The VIPAC MOX fuel was irradiated in some US test reactors and commercial LWRs and achieved burnups up to 285,000 MWd/t [Chidester et al., 2001]. The US VIPAC fuel development halted because of its lower smear density. Russian experience with VIPAC fuel was later revealed [Mayorshin et. al., 2000]. Extensive tests on VIPAC MOX fuel were performed back in the 1970's in BOR-60 and BN-600 fast reactors at the Research Institute of Atomic Reactor (RIAR) in Russia. By utilizing the vibration packing manufacture technique, UO_2 particles can achieve a smear density of ~77%, with a small amount of metallic uranium powder, the smear uranium density can reach 85% of that of the UO_2 . [Chidester et. al., 2001,- Mayorshin et. al., 2000]

Irradiation tests of VIPAC fuel have shown less fuel-cladding mechanical and chemical interaction compared with the sintered fuel, due to the ability of the fuel particles to accommodate the strains and to the improved conductance at fuel-clad interface. By using uranium metal powders as a getter, the Russians were able to achieve fairly high smear density (9.0-9.4 g/cc) in some of Russian VIPAC fuel rods. It was also found that the presence of uranium powder was instrumental for control of the oxygen to metal ratio and to reduce the release of cesium. In the meantime, the fuel cladding chemical interaction was greatly reduced as well [Mayorshin et. al., 2000]. However, addition of uranium powder may enhance the fission gas release since the metallic fuel powder has higher fission gas release than that of ceramic fuel. In addition, the uranium powder melting during operation can be a concern due to its low melting point.

The disadvantages of VIPAC fuel involve greater amount of fission gas release at high burnup due to the enlarged open surface and sintering of the particles at high fuel temperatures [Fitts and Miller, 1974]. Also, the lower smear density achievable for the VIPAC fuel yields a shorter cycle length for fixed enrichment.

13.2 Reference Annular Fuel Geometry

The UO₂ internally and externally cooled annular fuel is able to fit in the current operating PWRs while achieving higher core power density. The assembly dimensions as well as the coolant to fuel ratio are the same as those of today's PWRs. Based on the Westinghouse PWR fuel assembly dimensions, a fuel cell from a 13x13 annular fuel reference assembly is illustrated in Figure 6.1 in Chapter 6. If VIPAC fuel is used, the fuel-clad gaps would be eliminated. The reference dimensions determined by thermal hydraulic analysis are given in Table 6.2.

13.3 Annular Fuel Model Development

13.3.1 Major tasks for annular fuel model development

In order to accurately predict the performance of the internally and externally cooled annular fuel during steady state operation, it is imperative to be able to calculate the following items as a function of time: (1) the temperature profiles in the fuel and the claddings, (2) the heat flux at the inner and outer coolant channels, (3) the fuel cladding mechanical interactions; for the sintered annular fuel, the fuel-cladding interaction takes place at the moment gap closure occurs while for VIPAC annular fuel, the fuel interacts with cladding throughout the irradiation period since there is no gap in a VIPAC fuel rod, (4) the fuel swelling, relocation, thermal expansion and densification, (5) the cladding elastic and plastic deformation, and (6) fission gas release and rod internal pressure.

The uniqueness of the internally and externally cooled annular fuel is embodied in its concentric inner and outer claddings, each of which separates the fuel from a thermal cooling channel. Therefore, the fuel temperature profile and mechanical interaction behavior are very different from today's commercial fuel. Furthermore, it is desirable to develop an annular fuel performance code based on an industry standard fuel performance code. The FRAPCON-3, which is a US Nuclear Regulatory Commission licensed code that calculates the steady state response of light water reactor fuel rods

during long-term burnup (up to 65GWd/MTU) [Berna et. al., 1997], has been selected as the basis for the I&E cooled annular fuel performance code development. Modeling and code development should also incorporate some new findings of high burnup fuel fission gas release and cladding corrosion. [Long et. al., 2002]

13.3.2 Annular Fuel Rim Effects & Radial Power /Burnup Model

Due to U-238 resonance capture of epithermal neutrons, the buildup of Pu-239 is significantly larger at the fuel surfaces facing the coolant. This leads to power peaking at both fuel surfaces and consequently higher burnup at fuel surfaces. Unlike the solid fuel with power peaking at the outer surface, the internally and externally cooled annular has power peaks at both the inner and outer surfaces. Therefore, the local burnup at the inner and outer surfaces of the annular fuel is also expected to be higher. At the higher local burnup region, porous zones (typically 100-200 μm thick) are formed, and are characterized by fine grains of 0.1-0.5 μm and 1-2 μm gas bubbles uniformly distributed at sub-grain boundaries. [Long et. al., 2002]

Although the power shape and burnup distribution can be calculated using a complex neutron physics code, it is desirable to use a simple burnup model in the fuel performance analysis. To calculate the radial power profile in an I&E cooled annular pellet, a power/burnup distribution model has been developed for the fuel performance code based on TUBRNP. [Long et. al., 2002, Lassmann et. al., 1994]. It incorporates the resonance absorptions at both the inner and outer surfaces of an I&E cooled annular fuel model with an empirical fit to MCNP-calculated radial power profiles, discussed in Chapter 9.

The I&E cooled annular fuel burnup radial power/burnup model is compared to the MCNP calculations at various burnups in Figure 13.1. At 0 MWd/kgU burnup, the power shape is flat since there is no plutonium in the fuel. At 30 MWd/kgU burnup, the power peaks at both inner and outer peripheries become evident due to non-uniform formation of Pu-239. At 70 MWd/kgU burnup, the power peaks at fuel peripheries are about twice the value of the power in the interior of the fuel. An empirical fit is provided for annular fuel plutonium distribution shape function to capture the rim effects of annular fuel, where r_o and r_i are outer and inner radius respectively:

$$f(r) = 1.5 \cdot e^{-8.2(r-r_i)^{0.56}} + 1.74 \cdot e^{-7.3 \cdot (r_o-r)^{0.5}} + 0.26 \cos\left(\frac{r - \frac{r_o + r_i}{2}}{r_o - r_i}\right) \quad (13-1)$$

The power/ burnup model in FRAPACON-ANNULAR code achieves reasonable agreement with the MCNP calculations. Although there is a small discrepancy between the two calculations due to the crude nature of the former model, the effect on fuel performance is negligible.

Figure 13.1 Calculated Power Distributions by MCNP and FRAPCON-ANNULAR Model

13.3.3 The Annular Fuel Radial Temperature Profile Calculation

The generic steady-state integral form of the heat conduction equation is

$$\iint_S K(T, \vec{x}) \vec{\nabla} T(\vec{x}) \cdot \vec{n} ds = \iiint_V S(\vec{x}) dV \quad (13-2)$$

where K = thermal conductivity (W/m-K), s = surface of the control volume (m^2), n =the surface normal unit vector, S = internal heat source (W/m^3), V = control volume (m^3), x = the space coordinates (m), and T =temperature (K).

It is assumed that annular fuel has symmetrical heat transfer azimuthally with negligible heat conduction in the axial direction.

Two boundary conditions are used to solve the thermal conduction equation for the annular fuel:

$$(1) T_{inner_surface} = T_{inner_clad} + \Delta T_{inner_gap} \quad (13-3)$$

$$(2) T_{outer_surface} = T_{outer_clad} + \Delta T_{outer_gap} \quad (13-4)$$

For the sintered annular fuel, ΔT_{inner_gap} and ΔT_{outer_gap} are the inner and outer gap temperature drops. For VIPAC fuel, ΔT_{inner_gap} and ΔT_{outer_gap} are the temperature drops at

the inner and outer interface. For I&E cooled annular fuel, the boundary conditions are dictated by both the fuel inner and outer surface temperatures which can be obtained by calculating the cladding temperatures plus gap (or fuel-clad interface) temperature drops. With boundary conditions specified, the fuel radial temperature profile can be calculated and the fuel thermal mechanical responses corresponding to the temperature profile and irradiation history can be derived. For the sintered annular fuel, the gap sizes can be obtained by iterating on the temperature profile and the resulting dimensional changes. For VIPAC annular fuel, the iteration scheme is similar to that of the sintered annular fuel except that both the inner and outer gaps are zero. The steady-state integral heat conduction equation of the annular fuel is solved by a finite difference approach. The mesh points are arranged such that more meshing points are placed at the fuel regions with larger power gradient. The temperature profiles for the pellet and VIPAC 13x13 annular fuel are compared to that of the reference solid fuel on Figure 13.2. The temperatures are for the hot spot at a linear heat rate of 73kW/m and 45kW/m for annular and solid fuel, respectively. The results confirm the expected much smaller peak fuel temperatures for annular fuel than for solid fuel. Moreover, it can be seen that VIPAC fuel has smaller fuel temperature than the pellet fuel. This is due to higher contact conductance than the gap conductance of the pellet fuel. Furthermore, the temperature jump across the inner gap is larger than that for the outer gap, as a result of thermal expansion towards the outer cladding. This gap asymmetry will be discussed later.

Figure 13.2 Comparison of Peak Temperature Profiles of the Annular Fuel with the Reference PWR Fuel

13.3.4 Annular Fuel Cladding Thermal Mechanical Interaction

The fuel dimensional changes as a result of thermal response and irradiation effects have significant impact on fuel performance. The mechanisms that cause fuel dimensional changes include: (1) fuel thermal expansion, (2) fuel densification due to disappearance of small size pores during the early stage of irradiation, (3) fuel swelling due to build-up of solid and gaseous fission products during irradiation, and (4) fuel relocation due to the movement of cracked fuel. The fuel temperature profile and cladding behavior are affected by these dimensional changes due to the variations of fuel cladding gaps and Fuel-cladding Mechanical and Chemical Interactions (FCMI). At the initial stages of fuel operation, fuel densification causes shrinkage of the fuel and may be responsible for cladding failure due to cladding creep-down under the coolant pressure. At high burnup, excessive solid swelling becomes a major constraint for cladding performance. In order to describe the fuel behavior, it is important to accurately model the mechanisms for fuel dimensional changes.

(1) Annular Fuel Dimensional Changes

The fuel dimensional changes for a certain temperature profile are calculated by dividing the fuel into multiple rings and summing up the contributions of swelling, densification and thermal expansion from each ring. The inner radius can be approximated to the change with fuel ring volume averaged temperature as follows:

$$r_i = a + u_i = a[1 + \alpha \Delta \bar{T}] \quad (13-5)$$

This inner radius is used as an anchor to place the surrounding rings in such a way as to maintain radial continuity. The fuel ring hoop strain, due to thermal expansion, densification and swelling, is calculated from:

$$\varepsilon_t^i = \varepsilon_{thermal} + \varepsilon_{swelling} + \varepsilon_{densification} \quad (13-6)$$

where $\varepsilon_{thermal}$, $\varepsilon_{swelling}$, $\varepsilon_{densification}$ are the thermal expansion, swelling and densification strain respectively. For each ring, the inner radius is made equal to the outer radius of the adjacent inner ring and the ring radius is adjusted properly with the total strain.

(2) Annular Fuel Relocation

The temperature gradients in the fuel tend to cause cracks and these cracks promote fuel relocations that reduce the gap sizes. This process begins at BOL and quickly reaches equilibrium. [Oguma, 1983] The fuel cracks are mostly oriented in the radial direction. However, some circumferential cracking can occur, which tends to degrade the fuel thermal conductivity. For the solid fuel, the fuel relocation movement due to cracking is mainly outwardly since there is only one degree of freedom. However, for the annular fuel, the cracked fuel can move towards both inner and outer directions. Therefore, the relocation of annular fuel will result in reduction of gap sizes at both the inner and outer gaps if it happens.

Since the I&E cooled annular fuel is cooled at both sides and the fuel temperature gradient is expected to be modest, it is imperative to evaluate the fuel relocation. The I&E cooled annular fuel has been examined using a finite element analysis code ANSYS, and a typical radial temperature (73kW/m local power) was obtained. An empirical fit for relocation dependence on burnup was developed by adjusting the parameters in the original FRAPCON relocation model, using the temperature gradient as a base for adjusting relocation [Lanning et. al., 1995]

$$\Delta G / G(\%) = \begin{cases} 20 + 7 \cdot Fbu & \text{for LHGR} < 55\text{kW/m} \\ 19 + Pfactor + (8 + Pfactor) \cdot Fbu & \text{for LHGR} < 120\text{kW/m} \\ 22 + 12 \cdot Fbu & \text{for LHGR} > 120\text{kW/m} \end{cases} \quad (13-7)$$

where, $\Delta G / G$ = fuel-cladding gap reduction in percent

Fbu = Burnup/5.0, for Burnup < 5GWd/MTU

= 1.0 for Burnup $\geq$ 5GWd/MTU

$Pfactor$ = (LHGR-55)/4, LHGR is linear power in kW/m

For VIPAC annular fuel, the fuel relocation model is shut off since both the inner and outer gaps do not exist during irradiation. More details on the relocation model are given in [Yuan et. al., 2004].

(3) Annular Fuel Cladding Mechanical Interaction

The fuel cladding mechanical interaction plays an important role in fuel performance for several reasons: (1) the cladding stress and strain due to fuel cladding interaction may cause cladding failure and subsequent release of fission products, (2) the fuel cladding gap sizes and fuel cladding interfacial pressures (when gaps are closed) have significant impact on the heat transfer coefficient of the fuel-cladding interface, (3) for the internally and externally cooled annular fuel, the gap heat transfer resistance imbalance due to fuel-cladding mechanical interaction is one of the major constraints for fuel development. Therefore, an accurate prediction of the fuel cladding mechanical interaction by the performance model is critical for identifying the fuel safety margin and optimum fuel design.

Fuel-cladding mechanical interaction is a time dependent process which involves major fuel and cladding behavioral parameters. For the solid fuel, the fuel experiences instantaneous thermal expansion and the fuel-cladding gap size is quickly reduced at the Beginning of Life (BOL). During the first cycle, the difference between cladding inside and outside pressures generally causes the cladding creep down towards the fuel. Meanwhile, the irradiation induced densification causes fuel shrinkage during the first few thousand hours of reactor operation. The combined effects of cladding creepdown and densification dictate the gap size. The relocation effect in thermal calculations is not considered in the fuel-cladding mechanical interaction due to its “soft” nature. At higher burnup, with the production and release of fission gases (mainly Kr and Xe), the rod internal pressure becomes higher, which tends to retard cladding creep down. The swelling induced by fission products comes into play and the fuel cladding gaps will be closed during a long fuel operation cycle. When the fuel-cladding gap is closed, the fuel

expansion driven by swelling leads to build-up of the fuel cladding interfacial pressure, which may eventually cause cladding failure at high burnup.

The fuel cladding mechanical interactions of the annular fuel are even more complicated because there are two fuel-cladding gaps and the interaction at these two gaps of the cladding with the fuel are more complex and difficult to predict. Despite the complexity of development of gap sizes and fuel-cladding contact conditions, for annular fuel cladding interactions, in principle three physical situations can be envisioned:

1) Free standing cladding regime:

At this regime, both gaps are open and the cladding stress and strain are purely dictated by the rod internal pressure and the external coolant pressure.

2) Single gap closure regime:

At this regime, one gap is closed while the other gap remains open. The claddings are treated as free standing claddings but the fuel movement will proceed towards the side where the gap is still open.

3) Fuel-cladding full contact regime:

At this regime, both gaps are closed and the interfacial pressures are built up due to swelling.

A fuel cladding mechanical interaction model for internally and externally cooled annular fuel has been developed based on the FRACAS model, which is a pellet cladding interaction analysis model [Bohn, 1977]. The FRACAS model has been widely used in the industry since it was developed and a simplified version of FRACAS is used in the FRAPCON-3 code.

For each time step, fuel dimensions are calculated considering fuel thermal expansion, swelling and densification. Cladding dimensions are also calculated considering thermal, irradiation and mechanical effects. From the fuel and cladding dimensions and their relative positions, a judgment regarding gap closure for both the inner and outer gaps is made. If both gaps are open, the code turns to the free standing cladding solution, where cladding stresses and strains are calculated with prescribed internal gas pressure and external coolant pressure. If at least one of the gaps is closed, then the load (including temperature, pressure and resulting dimensional changes) is applied progressively with small increments. At each increment, the gap closure judgments are made. If both gaps are still open, the free standing cladding conditions are calculated and the next increment is subsequently added. If one of the gaps is closed, the fuel will be radially moved within the claddings in such a way that at the contact point, the interfacial pressure is taken to be zero and the dimensional increment is added to the opposite fuel surface. If both gaps are closed, the solution will be derived for the two claddings. A diagram for the annular fuel thermal and mechanical interactions solutions is illustrated in Figure 13.3. The details of the model are given by Yuan et. al. [2004].

Figure 13.3 The Sintered Annular Fuel Thermal/Mechanical Interaction Model Diagram

(4) VIPAC Fuel Thermal Conductivity and Thermal Conductance Model

The Vibration Compacted (VIPAC) fuel thermal conductivity was measured and a theoretical model was developed for fast reactor fuel [Hall and Martin, 1981]. Calza-Bini et al [1974] measured the VIPAC $\text{UO}_2\text{-PuO}_2$ fuel back in early 1970's and developed a simple fit of the VIPAC thermal conductivity. VIPAC thermal conductivity was also measured at PNL and a simple model was provided as a function of temperature and fission content. [Cunningham et. al., 1981]

Both the theoretical models and empirical models of previous studies are somewhat inadequate for implementation in our VIPAC fuel performance code for the following reasons: (1) Theoretical models are difficult to implement while providing poor agreement with experiments. (2) Empirical models found in the literature generally lack important parameters that impact the VIPAC thermal conductivity, such as burnup, rod pressure, porosity, etc.

The Cunningham's relation is adopted here but with adjusted parameters to yield a better fit with data:

$$K(T) = 3.22 - 1.82 \times 10^{-3} (T - 273) + 4.23 \times 10^{-7} (T - 273)^2 \quad (13-8)$$

This relation is also extended to incorporate the pressure, burnup, and porosity effects. It is assumed that the VIPAC fuel conductivity degradation with burnup is the same as that of the pelletized fuel.

The following pressure enhancement factor was added to extrapolate the thermal conductivity to different rod internal pressures:

$$F_p = 1.1986 \times P^{0.23} \quad \text{where } P \text{ is pressure in MPa,} \quad (13-9)$$

A simple relation is derived to describe the degradation of the VIPAC fuel thermal conductivity due to porosity:

$$F_{por} = e^{-0.125 \cdot (\text{porosity} - 13)} \quad (13-10)$$

where, F_{por} is the porosity factor, porosity is VIPAC fuel porosity in percent.

The data obtained for different gas pressures and different smear densities [Hall and Martin, 1971; Calza-Bini et. al., 1974; Cunningham et. al, 1981] are compared to model predictions, as in Figure 13.4 and Figure 13.5.

Figure 13.4 Comparison of Data and Model at Different Gas Pressures

Figure 13.5 Comparisons of Data and Model at Different Smear Densities

Three components contribute to the VIPAC fuel-cladding conductance: (1) thermal conduction through the bonding gas which exists at the fuel-cladding interface, (2) heat transfer due to radiation and (3) conductance through the contact points of particles and cladding,

$$h = h_{\text{gas}} + h_{\text{radiation}} + h_{\text{contact}} \quad (13-11)$$

The VIPAC fuel gap conductance model uses Berna's gap conductance model for sintered fuel with modified gas pressure effects to fit the experimental data. A gas pressure enhancement term was added

$$F_{\text{Pres}} = 12.45 \times P^{0.23} \quad \text{where } P \text{ is pressure in MPa} \quad (13-12)$$

The model has been able to fit Calza-Bini data as shown in Figure 13.6.

Figure 13.6 Thermal Conductance Model Fitting of Calza-Bini Data [1974]

(5) Fuel Relocation Assumption and Fuel-Cladding Thermal Mechanical Interaction

The dimensional changes of the I&E cooled VIPAC annular fuel are affected by the following mechanisms: (1) thermal expansion due to temperature effects, (2) densification of the fuel due to the disappearance of small porosities by irradiation, (3) solid fission product swelling. The VIPAC fuel is different from the pelletized fuel in that it is composed of small particles which are readily adjusted to the cladding geometry.

Therefore, the models of the VIPAC annular fuel are different from those of the pelletized annular fuel.

Although cracks may develop within the fuel particles, the relocation model is not applicable to VIPAC annular fuel since no fuel cladding gaps exist and the fuel particles can be easily moved to fill the entire volume enclosed by the claddings.

The thermal expansion of the VIPAC fuel is also different from the pelletized annular fuel. The thermal expansion results in larger particle diameters and larger porosity, unlike the pelletized fuel, where thermal expansion causes outward fuel expansion. The VIPAC annular fuel thermal expansion moves the particles in both directions within the concentric claddings. Therefore, the bulk effect is that the fuel outer radius is enlarged and the inner radius is reduced due to thermal expansion.

The VIPAC fuel has large porosity that can accommodate the fission products. Therefore, swelling of the VIPAC fuel is much less than the pelletized fuel. The dimensional change caused by swelling is not seen until reaching high burnup.

For VIPAC Annular Fuel, the fuel and cladding interaction is very similar to the fuel cladding full contact regime of the sintered annular fuel. However, the “lock-up” mechanism is no longer valid for the VIPAC annular case since fuel particles are more mobile and a “slippage” to some extent is expected. Nevertheless, since the fuel particles are still closely packed, the cladding strain can be related to the fuel strain assuming a certain “slipping ratio”.

(6) Annular Fuel Fission Gas Release

For internally and externally cooled annular fuel, the fuel temperatures are significantly lower than those of solid fuel and remain mostly below 800°C. Therefore, recoil and knock-out are the main mechanisms for I&E cooled annular fuel fission gas release. Since the fission products have a traveling distance of 10-15 μm , it is possible that a fission fragment be rejected out of the fuel when fissions occur within 10-15 μm of the fuel surface. Fission gas atoms are either directly ejected to the free volume when it is still an energetic fragment (recoil) or knocked out by interactions with other fission fragments in a collision cascade. (knock-out) [Olander, 1974]

At low burnup, fission gas release from the annular fuel rod can be predicated by FRAPCON model [Berna et. al., 1997] with a surface area enhancement factor. At high burnup, cracks will develop and microstructure change will take place. The fuel surface exposed to the free volume will be enlarged significantly. Therefore, the effect of initial fuel geometry will be negligible and the fission gas release will behave like the solid fuel at high burnup.

At low burnups, the fission gas release from the VIPAC annular fuel rod can be calculated by adjusting of surface release effects and resolution effects. Compared with sintered fuel, the VIPAC fuel fission gas release is generally higher due to enlarged free surface. At the same particle size, the VIPAC fuel with higher smear density tends to release smaller amount of fission gas than fuels with lower smear density because the resolution of fission gas is more prominent for fuels with higher smear densities and the resolution effect offset some of the surface fission gas release contribution. Comparison of fission gas releases from pelletized fuel and VIPAC fuel is presented in Figure 13.7.

Figure 13.7 Fission Gas Release from Pelletized Fuel and VIPAC Fuels

13.4 Annular Fuel Performance

The annular fuel performance code FRAPCON-ANNULAR has been utilized to perform analysis of both the sintered and VIPAC I&E cooled annular fuel design. Comparisons have been made among the reference 13×13 I&E cooled annular fuel rods operating at 100% of current PWRs' power and at 150% power and a reference Westinghouse solid fuel rod operating at its rated power.

The internally and externally cooled annular fuels are assumed to have an average initial LHGR of 50.9kW/m (for 100% power) and 76.8 kW/m (for 150% power), which correspond to 100% and 150% core power density of the reference Westinghouse plant. It is assumed that the axial power shape is a chopped cosine curve with peak to average ratio of 1.3. The reference Westinghouse solid fuel rod operates with a LHGR of ~31kW/m. These rods are assumed to operate at the hottest location of the core. The power history is presented in Figure 13.8.

The 150% power VIPAC annular fuel average temperatures are compared with the sintered pellet annular fuel case of the same power level and the reference solid fuel with 100% power in Figure 13.9.

Figure 13.8 Fuel Power Histories for Annular Fuel

Figure 13.9 Comparisons of Average Temperatures of the Hot Rods of the VIPAC Annular, the Sintered Annular and the Reference Solid Fuel

The VIPAC and the sintered annular fuel average temperatures are lower than those of the solid fuel although the annular cases corresponding to 50% higher core power. During the first ~50 EFPDs, the VIPAC annular fuel average temperatures are lower than those of the sintered annular fuel. The VIPAC fuel has no fuel cladding gaps and thermal resistance at the fuel cladding interfaces are much lower, resulting in lower average fuel temperature although the thermal conductivity of the VIPAC fuel is lower than that of the sintered fuel. However, the sintered annular fuel cladding gaps close after 50 effective full power days, and the sintered annular fuel average temperature drops below that of the VIPAC fuel since the sintered fuel has higher thermal conductivity. Near the end of life, after about 1000 effective full power days, the difference between the VIPAC and the sintered annular fuel temperatures becomes much smaller because at high burnup, the build-up of the solid fission products reduces the VIPAC fuel porosity and increases its thermal conductivity.

For VIPAC and pellet high power density annular fuel, although the linear power is very high, the fuel average temperatures are still low and fission gas release is mainly from the knock-out and recoil of fission gas atoms. The fission gas releases of the VIPAC, the sintered annular fuel and the reference solid fuels are compared in Figure 13.10.

Figure 13.10 Comparison of Fission Gas Releases (and Rod Pressure at EOL).

As shown in Figure 13.10, the VIPAC fuel fission gas release is high compared with the sintered fuel (~12% at EOL for 150% VIPAC annular, ~6% at EOL for 150% power sintered annular and 3% for 100% reference solid fuel). The VIPAC fuel fission gas release in our case can be explained by the larger surface to volume ratio that facilitates fission atoms escaping the fuel by recoil and knockout.

The fission gas release from VIPAC fuel can be reduced by enlarging the fuel particle size (reducing the surface to volume ratio) or reducing the fuel porosity (allowing higher resolution rate). However, to attain high smear density, the particle diameter has to be smaller than 500 μm , at least for a portion of the fuel.

The VIPAC annular rod should be filled with helium gas to enhance the thermal conductivity of the VIPAC fuel and the conductance of the fuel-cladding interface. The VIPAC fuel thermal conductivity is sensitive to the gas pressure since the gas serves as a major medium of heat transfer between fuel particles. Therefore, initial pressure is optimized to enhance the thermal conductivity while not exceeding the EOL rod pressure limits. The optimal pressure is identified to be between 1.4-2.0 MPa, which is comparable to the internal pressure of the reference fuel.

Figure 13.11 Comparisons of the Average Cladding Temperatures

The cladding average temperatures for the VIPAC, sintered annular and solid reference cases are compared in Figure 13.11. The cladding temperatures for the VIPAC annular fuel and the sintered annular fuel are comparable. For both annular fuels, the inner cladding temperatures are higher than the solid reference rod cladding temperatures due to higher heat fluxes on the inner surface. However, the outer cladding temperatures for both annular cases are lower than the reference solid fuel. Since the external coolant

temperatures are assumed equal for all three cases and the operating periods are equal, the VIPAC cladding oxide thickness and hydrogen concentration are comparable to those of the sintered annular fuel.

The rod average cladding strains for all three cases are plotted as a function of EFPDs in Figure 13.12. At beginning of life (BOL) when the fuel is not in contact with both claddings, the sintered annular fuel experiences larger strains, at both the inner and outer claddings than the solid fuel due to larger rod diameter. Since there are no fuel-cladding gaps in a VIPAC annular fuel rod, the cladding strain during the initial operating period due to external pressure is more limited. At high burnup, the presence of larger porosity in the VIPAC fuel can accommodate the fuel solid fission products. Therefore, the effect of the VIPAC fuel-cladding mechanical interactions is expected to be less severe than the sintered fuel. Thermal expansion of VIPAC fuel causes positive outer cladding strains and negative inner cladding strains, while the effect of densification causes reduction of cladding strain at both claddings. The gradient of cladding strain increment over the operation period is much smaller for VIPAC annular fuel than for sintered annular fuel since the swelling effect of the VIPAC fuel is reduced by its larger porosity.

Figure 13.12 Comparisons of Cladding Strains

Compared with sintered fuel, the VIPAC fuel also provides a “softer” contact between the fuel and cladding, which effectively reduces the intensity of fuel-cladding mechanical interactions and avoids stress concentrations. Therefore, the VIPAC rod cladding performance is relatively better than the sintered annular fuel. Cladding strain remains

below the 1% limit for both the pellet and VIPAC fuel operating at 150% power, but the margin for the pellet fuel is reduced in comparison with that of the solid reference fuel.

13.5 Annular Fuel Design Optimization

13.5.1 Gap Conductance asymmetry in Sintered Annular Fuel

For sintered annular fuel, a gap size asymmetry is likely to develop. Hence, due to the resulting imbalance of gap conductances, the heat flux would be larger where gap conductance is larger, which could lead to impairment of the Minimum Departure from Nucleate Boiling Ratio (MDNBR) as listed in Table 13.1.

Table 13.1 Sensitivity of DNBR to Gap Conductance Asymmetry (see Chapter 6)*

Inner Gap conductance (W/m ² .K)	Outer Gap conductance (W/m ² .K)	Gap conductance Asymmetry**	MDNBR (inner)	MDNBR (outer)
6000	6000	1.00	1.373	1.928
5000	5500	1.10	1.591	1.791
5000	6000	1.20	1.776	1.67
7000	8000	1.14	1.592	1.789
7000	9000	1.29	1.792	1.649

*Assumes constant gap conductance axially, which ignore the random pellet-cladding eccentricity

**Ratio of outer gap conductance to inner gap conductance

A sensitivity study of the major parameters affecting the annular fuel performance was conducted. It has shown that a smaller as-fabricated inner gap size and larger outer gap could reduce the potential gap conductance asymmetry. The total gap size should be reduced and fuel volume should be enlarged to reduce the cladding strains due to cladding creepdown. Increasing the initial filled helium gas pressure produces little improvement in gap conductance imbalance; whereas a much higher initial gas pressure would be detrimental to fuel operation if it leads to reopening the fuel-cladding gaps with the addition of fission gases. The adjustment of inner and outer cladding thickness could allow a well designed allocation of swelling-induced cladding strains, but the resulting higher H₂ concentration and a higher fraction of oxidized thickness pose concerns for cladding failure. The reduction of the inner cladding thickness produces little improvement in gap conductance asymmetry.

Two approaches were identified as the most promising in resolving the gap asymmetry issue. These involve:

1. Using larger fuel and cladding roughness or sputtering the fuel surface with ZrO₂ particles.
2. Employing a smaller as manufactured inner gap and a larger outer gap to provide more space for thermal expansion towards the outer cladding.

These two approaches will be described in more detail in the following two sections.

13.5.1.1 Fuel Pellet Surface Treatment Approach

Larger fuel roughness effectively enlarges the gap size of the outer gap therefore reducing the gap imbalance. A thin layer of sputtered ZrO_2 particles has a large thermal resistance and dominates the gap conductance, therefore the gap sizes are no longer important. The ZrO_2 layer has to be crushable, i.e. should have high porosity (~50%). When fuel cladding contact is made, the interfacial pressure could crush the ZrO_2 particles and thermal conductance between fuel and cladding can be improved. Therefore, the ZrO_2 sputtering option is found to be effective in reducing the gap conductance asymmetry, as shown in Figure 13.13. A 15 micron particles sputtered over about 50% of the outer surface were identified to yield the minimum gap conductance asymmetry. It is also noted that the large fuel cladding roughness and the sputtered ZrO_2 particles were also found to be effective in reducing the strains due to cladding creep-down to the fuel.

Figure 13.13 Impact of ZrO_2 Sputtering on Gap Conductance Asymmetry

13.5.1.2 “Tight and Loose” Inner and Outer Gap Approach

Since the sintered fuel expands upon heating towards the outer cladding, it is desirable to engineer the inner gap to be smaller and the outer gap to be larger to reduce interfacial contact between the pellet and outer cladding at closure. This approach is found to be feasible in view of the manufacturing results (see Chapter 11), which suggest very small inner gap tolerances can be achieved. The best tolerances achievable by manufacturing

are summarized in Figures 13.14 and 13.15 for the inner and outer gaps, respectively. Note that the nominal inner gap was reduced from the originally assumed 0.06 mm of PQN-02 design to 0.03mm and the outer nominal gap was increased from the original 0.06mm to 0.08mm. Therefore, the fuel volume loading has been preserved, i.e.

$$\text{Original volume } V_{f-\text{orig}} = \pi / 4 * (D_{fo}^2 - D_{fi}^2) = \pi / 4 * (1.41^2 - 0.99^2) = 0.793 \text{ cm}^3$$

$$\text{New volume: } V_{f-\text{new}} = \pi / 4 * (D_{fon}^2 - D_{fin}^2) = \pi / 4 * (1.37^2 - 0.93^2) = 0.7958 \text{ cm}^3.$$

In addition, the effect of a random axial distribution of gap sizes was evaluated using a random number generator, a random distribution of the inner and outer gaps sizes along the rod (for each axial node for MDNBR analysis) was created. The results are shown in Figure 13.16. It can be observed that the gap widths remain within the maximum – minimum range and that the gap sizes oscillate around the nominal values of 0.03 and 0.08mm, as expected.

To calculate gap conductances for various gap sizes, the FRAPCON-ANNULAR code was used. Since the worse case is at BOL, only BOL values were calculated. The set of gap conductance values for various gap sizes is plotted in Figure 13.17. It can be observed that the smaller the outer gap the larger is the fraction of the outer gap that is closed and that exhibits high conductance due to contact pressure – an undesirable situation for MDNBR. The sudden reduction of inner gap conductance at about 1m is due to relocation caused by the closure of the outer gap at the locus of 1m.

Figure 13.14 Best achievable tolerances on the inner gap

Figure 13.15 Best achievable tolerances on the outer gap

Figure 13.16 Random distribution of the inner and outer gap size

Figure 13.17 Gap conductances for various gap sizes

In the next step gap conductance profiles calculated by FRAPCON-ANNULAR, which incorporate thermal expansion, the heat flux profile effect and all other effects modeled in FRAPCON-ANNULAR were combined with the effect of random distribution of manufacturing tolerances. This has been accomplished through a separate computer program developed for this purpose. It reads FRAPCON-ANNULAR generated gap conductance profiles, calculates random gap size distribution (Figure 13.16) and combines this distribution with gap conductance profiles from Figure 13.17 using linear interpolation in 2-D space. Interpolation in z-coordinate is necessary because FRAPCON-ANNULAR does not allow more axial nodes than 18 and the preferable number of nodes for MDNBR analysis is above 40 (48 nodes is used). The results are plotted in Figure 13.17, where the node number in the x-axis designates node position from core bottom.

The MDNBR analysis at 150% power using the gap conduction distribution from Figure 13.17 results in MDNBR values in the inner and outer channel (1.59 and 1.47, respectively) that are well balanced and correspond closely to MDNBR value for the reference solid fueled core at 100% power.

These results suggest that for the cold gap nominal dimensions of 0.03mm and 0.08mm for the inner and outer gaps, provided that the tolerances of ± 0.02 mm (diametral) can be attained for both the inner cladding and pellet diameters, satisfactory MDNBRs can be reached without the need for sputtered zirconia coating layers. This also assumes that the minimum radial gap that allows for the assembly is in the worst case (inner clad OD $+0.02$ mm and fuel pellet ID -0.02 mm) 0.01mm. It is important to note that these tolerances are critical for the inner gap. It is expected that relaxing the diametral

tolerances on the outer gap side to $\pm 0.04\text{mm}$ would not have significant effect on the MDNBR performance.

Figure 13.17 Gap conductances along annular rod including both random tolerances and heat flux profile

13.5.2 Optimization of VIPAC fuel

A sensitivity of the important parameters of the VIPAC fuel has been performed to identify the optimum values of these parameters. The initial helium gas in the fuel rod plays an important role in improving the conductivity of the fuel. At very low gas pressures, the thermal conductivity is low and the fission gas release is rather high. At high pressures, the improvement of the thermal conductivity does not reduce the fission gas release since the gas release is mainly activated by athermal mechanisms, therefore the EOL rod pressure becomes very high. Hence the optimum initial helium gas pressure is found to be in the range of 1.4-2.0 MPa.

VIPAC fuels of different particle sizes have also been evaluated. A larger particle size reduces the fission gas release by reducing the surface to volume ratio, yet it increases the jump distance which leads to larger temperature drop at the fuel cladding interface. The optimum particle size has been identified to be around 300-600 μm in diameter. In practice, a range of particles sizes should be used with the smaller particles being about 30-60 μm in diameter. This will improve the VIPAC fuel smear density and conductivity.

Larger smear density results in higher thermal conductivity and improves fission gas resolution rate. The fission gas release reduction by improving the smear density is significant at lower smear densities and at high smear densities the reduction is modest. However higher smear densities limit the accommodation capability of released fission

gases. The optimum smear density would be in the range of 85%-90%, if it can be achieved during manufacturing. The experiments with manufacturing about 1m VIPAC annular rod at AECL suggest that achievement of such high densities in a narrow annulus will not be possible without uranium metal powder, as discussed in Chapter 11.

The VIPAC fuel performance can be improved by addition of uranium metal powders in the particle bed. Due to high thermal conductivity of the uranium powder, it improves the bulk fuel thermal conductivity and the presence of fine particles at the fuel cladding interface improves thermal gap conductance. The uranium powder increases the heavy metal content in the fuel and serves to control the oxygen potential in the fuel, hence reduces the fuel cladding chemical interaction. However, the uranium powder tends to release all the fission gas at high burnup and its compatibility with water is a concern. Further investigation of this design option is recommended.

13.6 Summary and Conclusion

13.6.1 For sintered annular fuel:

An annular fuel performance modeling capability was developed within the FRAPCON code. It is found that the annular fuel rods have lower fission gas release than the reference fuel at the same power density. Higher fission gas release is predicted at 150% core power density at the same operation time. The EOL fission gas release from a 150% power annular fuel is about 6%, which is still tolerable with good plenum and cladding design. Even at higher power density, the hydrogen concentration and the oxide accumulation of the annular fuel are comparable to those of the solid fuel since the cladding heat flux and irradiation time for annular fuel are not much different from the reference solid fuel. The annular fuel gap conductance asymmetry caused by outward fuel thermal expansion has been identified as a major concern due to its potential effect on MDNBR.

A sensitivity study has shown that a smaller as-fabricated inner gap and a larger outer gap can reduce or even eliminate the gap conductance asymmetry. Meanwhile, the total gap size should be reduced to decrease the cladding strains due to cladding creep-down. Adjustment of the inner and outer cladding thicknesses could allow acceptable swelling-induced cladding strains, but higher H₂ concentration and higher fraction of oxide layer pose concerns about cladding failure. Reduction of the inner cladding thickness shows little improvement of gap conductance asymmetry.

The gap asymmetry problem has been analyzed and several approaches are recommended: (1) adjusting the gap sizes to allow a larger outer gap and a smaller inner gap, (2) enlarging the fuel and cladding surface roughness, and (3) sputtering on the fuel surface with ZrO₂ particles. The above three approaches can be combined to reduce or eliminate the gap imbalance. The optimized annular fuel is analyzed at different power levels and shows great potential for achieving high burnup (up to 86MWd/kgU rod average) without compromising annular fuel safety.

13.6.2 For VIPAC fuel:

Special new models for VIPAC fuel properties were introduced in the FRAPCON code. It was found that the 150% power annular VIPAC fuel would operate at lower fuel temperatures than the sintered annular fuel for an initial period due to elimination of the gap temperature drops. After the sintered annular fuel gap closure, the sintered fuel would have a lower fuel temperature than VIPAC fuel due to higher fuel thermal conductivity. Fission gas release from VIPAC fuel is higher than both the sintered annular and the solid rods since its particle bed composition provides larger surface area and larger porosity which facilitate fission gas release. However, the end of life internal pressure of the VIPAC fuel is expected to be lower than the sintered annular fuel due to the fact that the VIPAC fuel has larger free volume.

The VIPAC annular fuel EOL cladding oxide thicknesses and hydrogen concentrations are comparable to those of the sintered annular fuel since the irradiation durations are the same and the cladding temperatures are not much different. The VIPAC annular rod cladding strains are significantly reduced for two reasons: (1) elimination of the gaps restrains the fuel cladding creep-down due to cladding pressure differential and (2) the large porosity accommodates the fuel solid fission products at high burnup. Another obvious advantage for the VIPAC annular fuel is that it eliminates the gap conductance imbalance problem which is a serious concern for the sintered annular fuel.

A sensitivity study of the important parameters of the VIPAC fuel has been performed to identify the optimum values of these parameters: the optimum initial helium gas pressure is around 1.4-2.0 Mpa; the optimum particle size is around 300-600 μ m; The optimum smear density is in the range of 85%-90%.

The VIPAC fuel performance can also be improved by the addition of uranium metal powders in the particle bed.

In general, the VIPAC shows good behavior in terms of cladding strains and gap balance. The fission gas release is larger than sintered fuel but could be accommodated by the large free volume in VIPAC fuel rod. The VIPAC fuel can be operated to rather high burnup (~ 97 MWd/kgU).

14. IRRADIATION PERFORMANCE

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The preceding chapters have described the motivation for development of internally and externally cooled annular fuel (IXAF) and have detailed the design and performance analysis of this fuel using a variety of analytical tools. This chapter describes an irradiation test of two samples of IXAF that were manufactured by Atomic Energy of Canada Ltd. (AECL) using vibration packing (VIPAC) techniques as described in Chapter 11. The goals of the irradiation test were to support the analytical and modeling effort by providing data on the actual performance of IXAF, at least at low burnup, and to serve as a demonstration that high power density annular fuel could be operated at the temperatures necessary for application in PWRs. Because there is limited or no experience the operating fuel, particularly VIPAC fuel, in the temperature regimes planned for the commercial application of IXAF, this initial irradiation test is a vital first step in providing data for continued development of high power density annular fuel. Finally, the test was also motivated by a desire to identify any problems with annular fuel that were not anticipated in the design process. The content and organization of the paper are introduced below.

In order to irradiate fissile material in an experimental facility at the MITR-II research reactor, an amendment to the reactor license was required. Because of the distinction between research and test reactors, even with this amendment the fuel test facility was required to operate using the reactor primary coolant for heat rejection. That is, the samples could not be installed in an independently cooled test loop. The requirements and considerations for performing the fuel test in the MITR-II have been described previously, with an emphasis on the licensing requirements and design basis for generic fuel irradiation facilities [Hu et al., 2005]. In this chapter, the irradiation capsule design is treated in detail with an emphasis on how the test design was targeted for the expected commercial application environment of annular fuel. In particular, results of design calculations and pre-irradiation testing that were employed to meet thermal requirements in the absence of active temperature control are presented.

The irradiation test itself consisted of two phases, an initial short-term test of a single fuel sample to demonstrate the capability of the facility, followed by approximately six months of irradiation of the initial sample and one additional specimen. Temperature histories for the two samples are presented. Additional analysis and special operating procedures were required as a result of temperature asymmetries that were observed between the inner annulus clad temperature and the outer clad temperature. The origin and significance of these asymmetries is discussed. In addition, the results of gamma spectrum analysis of cover gas samples taken periodically during the irradiation test are summarized.

Following irradiation and a decay period to allow the samples to be handled in hot cell facilities developed at MIT for the purpose, the samples were scanned for gamma activities. A collimated counting setup was used to measure the principal activities as a function of position across the fuel and gas plenum regions of the samples. The results of

these gamma scans are presented and discussed, particularly as they relate to burnup and fission gas release estimates for the fuel. The chapter concludes with a summary of the important results and recommendations for future irradiation testing of high power density annular fuels.

14.1 Irradiation Facility and Capsule Design

14.1.1 MITR-II Research Reactor and Experiment Constraints

The irradiation test of annular fuel samples was planned for the MITR-II 5 MW research reactor operated by the Massachusetts Institute of Technology Nuclear Reactor Laboratory. The power density and therefore the neutron flux are similar to those in commercial light water reactors [Kazimi et al., 2001]. This, coupled with the accessibility of the reactor core and the availability of infrastructure necessary for handling small sample segments of irradiated fuel made the MITR-II an attractive test bed for the annular fuel test. Figure 14.1 is a cutaway view of a solid model of the reactor core with a generic in-core experiment installed.

Figure 14.1 Cutaway view of the MITR-II core tank with an in-core experiment installed. (Fuel elements not shown.)

As noted above, however, it was necessary to obtain a license amendment for the reactor to permit irradiation of fissile material in an experimental facility. The design basis for this amendment is described in detail in Hu et al. [2005]. There are a number of significant constraints on a fissile material irradiation experiment in the MITR-II established by the Nuclear Regulatory Commission definition of a research reactor, by the license amendment authorizing these experiments and by the technical specifications relevant to all in-core experiments at the MITR-II. These are: the fuel may not be contained in a “circulating loop through the core”, the fissile content is limited to 100 g U^{235} or equivalent, the fissile material must be doubly encapsulated, the reactivity worth of the experiment may not exceed 1.8 % $\Delta K/K$ (2.3 beta), onset of nucleate boiling (ONB) on surfaces exposed to the MITR-II primary coolant must be avoided, and provision must be made to sample for fission product gas at least weekly during the irradiation and for redundant automatic overtemperature reactor scrams. The design process and resulting annular fuel irradiation facility design meeting these criteria is described below.

14.1.2 Annular Fuel Irradiation Facility Thermal Design

Avoiding the use of a separate circulating coolant loop for a fuelled experiment has a large impact on the design, particularly when high power density is desired. MITR-II reactor coolant is water at a core outlet temperature of about 50 °C and close to atmospheric pressure. It is therefore necessary to provide a heat transfer gap that will transfer the power generated by the fuel at a temperature differential of about 250-270 °C in order to operate the fuel at the temperatures expected in commercial application. This requirement is made more stringent by the limited in-core space available, which dictates that gap widths be on the order of 1-2 mm. It is also necessary to avoid temperature gradients so high that result in large temperature variations due to achievable manufacturing tolerances. Possible materials were evaluated for use in the thermal gaps. In general, conductivities for gases resulted in impractically small gaps at adequate heat rejection rates, solid materials required gaps larger than could be accommodated in order to provide sufficient temperature gradient, while packed particle beds raised concerns about uniform filling and mechanical behavior in small gaps. Lead bismuth eutectic was chosen as a heat transfer medium in order to overcome these problems. Its heat transfer coefficient is in the right range to achieve the desired temperature drop at a realizable gap width. Because it melts at about 130 °C, it would be in the liquid state at the planned clad temperatures and thus avoids the issue of gap contact and conductance at the fuel surface. It has the further advantages of low vapor pressure at the planned operating pressure and small changes in volume and heat transfer coefficient with phase change from solid to liquid.

As discussed in Chapter 6, the optimum square lattice annular fuel array is expected to be 13x13 in order to allow a 50% power uprate while maintaining DNBR margins and allowing for shift of heat flux to the outer clad surface. It proved impossible to use these fuel dimensions with lead bismuth eutectic heat transfer gaps and an aluminum outer capsule isolating the LBE and fuel samples from the MITR-II coolant. The inner coolant channel was too narrow to provide adequate coolant flow at the pressure drop available across the MITR-II core. Consequently, the best sample fuel dimensions were chosen to maintain the same thickness as the planned commercial fuel and the linear heat rate was

increased in order to produce a similar temperature profile. This thermal design task was accomplished using MCNP to calculate the neutronics and heating of the sample fuel and a customized one-dimensional heat transfer code – TAFIX – to calculate fuel temperature profiles and coolant thermal parameters. The details of TAFIX are given in Appendix I of Kazimi et al, [2001] Table 14.1 gives the dimensions of the 13x13 reference fuel design together with those of the MITR-II irradiation sample fuel.

Table 14.1 Key cross-section dimensions of the reference pellet fuel design and the VIPAC test samples irradiated in the MITR-II – dimensions in mm.

Dimension	Reference Pellet Fuel	VIPAC Test Fuel
Inner Clad ID	8.63	23.62
Inner Clad OD	9.78	25.62
Fuel ID	9.90	25.62*
Fuel OD	14.10	29.75*
Outer Clad ID	14.22	29.75
Outer Clad OD	15.37	31.75

*Note that the VIPAC fuel has no fuel/clad gap.

Linear power for VIPAC test fuel samples as a function of position in the MITR-II core was calculated using an MCNP4C [Briesmeister, 2000] model of the core and the results are shown in Figure 14.2 for beginning of life at 5% U^{235} enrichment and a smear density of 90%. Note that most of the neutrons causing fission in the test fuel arise from the MITR-II reactor fuel and this gives rise to the axial variation observed in linear power. A linear heat generation rate of 110 kW/m was targeted for the sample fuel. Based on the volume ratio of the sample to the reference commercial fuel this is equivalent to 50 kW/m in the commercial fuel, which is representative of the highest power fuel pin for a core operating at 150% of the rated power of a core based on a current 17x17 Westinghouse fuel assembly. The calculated linear heat rates for the VIPAC samples in the core center are close to this target. The samples at core top and bottom are below this value because lower neutron fluxes in these regions.

Figure 14.2. Linear power calculated using MCNP for four VIPAC fuel samples at various axial locations in the MITR-II.

The linear heat rates shown above were used to perform thermal/hydraulic analyses of the sample fuel in aluminum irradiation capsules with LBE heat transfer gaps. The irradiation capsule design is shown in Figure 14.3. Details of the structure of the test fuel samples are given in Chapter 11. Note the fueled region of the samples as indicated in Figure 14.3 – the fuel height is approximately 7 cm. The major components of the irradiation capsule as shown in Figure 14.3 are: the aluminum capsule structure consisting of inner and outer tubes with the integral end caps, upper and lower ceramic spacers to maintain centering of the sample, the LBE-filled gaps and the cover gas and instrumentation tube. Further details of the irradiation capsule construction and assembly are given in the following section.

Figure 14.4 compares the temperature profile calculated for the reference commercial fuel and the VIPAC samples in the irradiation capsules shown in Figure 14.3. The reference fuel profile is shown from the outside diameter (OD) of the inner clad, across the fuel/clad gap, through the fuel and across the fuel/clad gap to the inside diameter (ID) of the outer clad. For the sample fuel the complete temperature profile is shown from the water-cooled ID of the inner aluminum capsule, through the inner aluminum capsule wall, the inner LBE gap, the inner clad, fuel, outer clad, outer LBE gap, outer aluminum capsule wall to the water cooled OD of the outer aluminum capsule.

Figure 14.3. Fuel sample irradiation capsule design. The vertical line in the fuel sample indicates the approximate height of the fuel region within the clad fuel sample.

Figure 14.4. Comparison of temperature profiles for a power reactor reference annular fuel and sample fuel as irradiated in the MITR-II.

In order to calculate the temperature profile for the sample fuel, an effective conductivity for the LBE gaps must be input to the TAFIX code. Accurate results are important because there is no active control available for the sample temperatures. They are set by the geometry and composition of the irradiation capsule in conjunction with the fuel loading and the neutron flux produced by the MITR-II. In general, if actual temperatures are higher than calculated temperatures, safety limits might be reached as discussed below, preventing operation of the experiment. Actual temperatures lower than calculated temperatures are less interesting as a test of the performance of the annular fuel design. It was therefore deemed important to measure the effective LBE gap conductivity before finalizing the design of the irradiation experiment. These measurements were made using electrical heaters and LBE gaps in a variety of water-cooled and air-cooled configurations as described in Carpenter and Kohse [2005]. Practical constraints prevented the achievement of heat fluxes equal to those expected in core and the geometry of the inner annulus was not simulated. However, a range of heat fluxes and LBE thicknesses was investigated in order to increase confidence in the measured values. In general, effective gap conductivities were found to be dependent on heat flux and to be lower than reported values for the conductivity of LBE alone. This was attributed to the fact that the cooled aluminum capsule walls (or equivalent in the simulation experiments) operate below the melting point of the LBE and the gaps therefore consist of a solid LBE section in contact with aluminum and a liquid LBE section in contact with the heater or operating fuel sample. Several gap resistances are therefore introduced, and the interfacial pressure of the solid aluminum/solid LBE gap, in particular, is not well controlled. A clear trend of decreasing gap conductivity with increasing gap temperature was observed in all the experiments, although the reasons for this trend remain unclear. A value of gap conductivity of 5 W/m-K was adopted for the calculations of expected in-reactor behavior, with the expectation that this value would be, if anything, slightly conservative.

The fuel temperature profile shown above is the most important consideration for the relevance of the irradiation experiment to predicting annular fuel behavior in a commercial reactor. Clad behavior was not targeted because of the absence of pressurized water coolant in contact with the clad. MITR-II reactor safety considerations imposed some additional concerns, the most important of which is the avoidance of onset of nucleate boiling (ONB) on in-core surfaces. Figure 14.5 shows the axial temperature profile for the bulk coolant in the inner and outer flow channels as well as the aluminum surface temperatures in these channels, the saturation temperature and a prediction of the ONB temperature. From these results, it was concluded that adequate margin to ONB would be maintained if the measured temperatures were found to agree with the calculated values. In practice, this determination was complicated by uncertainties in the measured aluminum surface temperatures and by unexpected asymmetries between the outer and inner gaps as discussed in Section 14.3.2, below.

Figure 14.5. Bulk and surface temperatures in the annular fuel irradiation rig for four fuel samples. Saturation temperature and temperature at onset of nucleate boiling (ONB) are also shown.

The calculations presented here were used to validate the final design of the irradiation capsule with dimensions as given in Table 14.1. They were also used as the basis for the safety analysis that was presented to and approved by the Reactor Safeguards Committee. The case was based on maintaining measured fuel clad temperatures below 400 °C and, because ONB calculations are somewhat uncertain, the more conservative approach of maintaining wetted surface temperatures below the saturation temperature was adopted.

14.1.3 Mechanical Design and Assembly of the In-Core Facility

The overall design of the irradiation capsules was schematically indicated above in Figure 14.3. In this section, some additional details of the construction and assembly of the capsules are presented. The assembly of the capsules was accomplished as described in the following. First, the cover gas and instrumentation tube was brazed to the outer capsule end-cap region (note that the upper end cap was machined as an integral part of the outer capsule tube and the lower end cap as an integral part of the inner capsule tube in order to minimize the number of welds required for assembly. Two thermocouples (1.6 mm diameter SS sheath K-type) were threaded through the tube and attached to the fuel sample using spot welded zirconium foil strips as shown in Figure 14.6. The sample, ceramic spacers and LBE pre-forms were then assembled onto the inner capsule tube with integral lower end cap and inserted into the outer capsule tube with integral upper

end cap. During the insertion, the thermocouples were retracted through the cover gas/instrumentation tube.

At this point, the assemblies were seal welded at the junctions of the inner and outer capsule tube/end cap sections. Test welds had been performed on identical assemblies to verify the welding procedure with satisfactory results. However, during the welding of the full assemblies, a number of the seal welds were found to leak at the weld joining the upper end of the inner tube to the upper end cap. Two out of six assemblies were successfully sealed and passed a bubble pressure test with no indication of leakage. These two assemblies were used in the irradiation testing. Following seal welding and pressure testing, the assemblies were heated in an oven to a temperature of 165 °C for one hour in order to melt the LBE and ensure that the fueled section of the sample was completely immersed. The capsules were X-rayed at this point for verification of LBE coverage as shown in Figure 14.7. At this point, one of the capsules was equipped with four thermocouples on the outer aluminum surfaces in order to verify the thermal calculations that indicated the saturation temperature would not be exceeded. Two thermocouples were installed at the fuel region centerline, one on the inner and one on the outer surface. The remaining two were on the inner and outer surfaces at the top of the LBE region.

Figure 14.6. Annular fuel sample shown with thermocouples attached and assembled onto the inner capsule tube/end cap with the upper and lower ceramic spacers in place.

Figure 14.7. X-ray radiograph of a completely assembled irradiation capsule.

Installation in the MITR-II reactor core was accomplished by removing a fuel element or solid aluminum dummy fuel element from a core position and installing the experimental facility in its place. Design requirements for the irradiation facility were imposed by safety and technical specification requirements for the reactor as well as experimental goals. Total fissile content limits and the prohibition on the use of a separate circulating loop for cooling fuel samples have been discussed above and were major drivers for the thermal design. In addition, any in-core experiment must meet reactivity limits. If the experiment is secured in a manner equivalent to the reactor fuel elements, that limit is 1.8 % $\Delta K/K$. MCNP calculations showed that this limit is met even for the case of four irradiation capsules of the design shown here, leaving only the consideration that the capsules must be secured in an appropriate manner. Disruption of the coolant flow patterns in the core attributable to the installation of the experiment must also be considered – each fuelled element position must receive a certain minimum fraction of the total core flow. The primary experimental considerations for irradiation of the fuel were established by the neutronic and thermal/hydraulic calculations discussed above. That is, the fuel samples should operate at a linear power consistent with producing a prototypical temperature profile in the fuel and with achieving burnups relevant to evaluating the eventual performance of commercial fuel. Additionally, a design goal was established to allow for independent removal and replacement of any individual sample. These goals were all met by the individual sample capsule design detailed above.

It remained to design a facility to position the capsules in the core and to direct a sufficient fraction of the reactor coolant flow into the inner and outer coolant channels of the irradiation capsules. A cutaway view of the solid model of the specialized “dummy element” that accomplishes this is shown in Figure 14.8. In addition to the irradiation capsules, the important features of dummy element are the inlet and outlet nozzles and the locking pin. The inlet nozzle was permanently installed in the dummy element, while the irradiation capsules and the outlet nozzle were stacked in the central channel and held down by the locking pin. Rotational positioning and centering were provided by the guide pins that interlock with the channels provided in the dummy element. Channels were also provided for the cover gas and instrumentation tubes. The dummy element is made to install in-core in exactly the same way as regular fuel element, and the locking pin secures the stack and is, in turn, secured by the rotating grid plate that secures the

reactor fuel. When the reactor is shut down and the core tank is opened, any individual irradiation capsule can be retrieved by removing the locking pin and extracting the outlet nozzle and irradiation capsules in order. Specialized remote tooling was designed and constructed for this purpose, and storage locations within the reactor core tank are available for capsule shuffling or for longer term storage of a capsule for decay before removal from the core tank. Dummy irradiation capsules are available for cases where fewer than four fueled irradiation capsules are to be irradiated.

Figure 14.8. Cutaway view of four irradiation capsules installed in the dummy element with inlet and outlet nozzles.

Figure 14.9 is a photograph of the top of the dummy element with the locking pin and outlet nozzle visible, while Figure 14.10 is a photograph of the full rig installed in the MITR-II core tank with a single occupied irradiation capsule. Note in Figure 14.10 that the cover gas/instrumentation tube extends from the dummy fuel element to a point above the reactor core water level and it is supported by a bridge extending from the core tank wall. At the end of the tubing, the fuel sample clad thermocouples are brought out of the cover gas environment through a Conax® fitting. This irradiation capsule shown in Figures 14.8 and 14.9 was equipped with the water-side thermocouples described above – they can be seen routed up the outside of the cover gas tube.

Figure 14.9. Photograph of the upper end of the dummy fuel element showing the outlet nozzle and locking pin.

Figure 14.10. Photograph of the annular fuel rig installed in the MITR-II core.

14.2. Irradiation Test History

An initial short-term irradiation of several hours was made with a single irradiation capsule to verify the neutronic and thermal calculations before proceeding with a longer term irradiation. The rationale for using a single specimen was two-fold. First, total heat input to the facility is less for a single capsule, reducing the probability of reaching prohibited thermal/hydraulic conditions in the event that there were serious errors in the calculations. Secondly, if the single capsule proved to operate in a regime that was either prohibited due to temperatures being too high, or uninteresting due to temperatures being too low, the capsule geometries for the remaining specimens could be adjusted on the basis of the experimental data to target the desired temperatures more closely. In practice, the single capsule test showed temperatures somewhat lower than targeted, but with large asymmetries between the inner and outer clad surfaces. In light of uncertainties arising from the startup testing (see below) we decided to proceed with the longer term irradiation using the two available capsules. A number of important parameters for the two fuel samples irradiated are summarized in Table 14.2. The longer term test was terminated after approximately six months of operation due to a cover gas leak from one of the capsules.

Table 14.2 Summary of irradiated fuel sample characteristics

Parameter	Capsule 3	Capsule 4
Inner Clad ID-OD (mm)	23.66-25.59	23.67-25.60
Inner Clad Height (mm)	99.81	99.88
Outer Clad ID-OD (mm)	29.75-31.83	29.76-31.83
Outer Clad Height (mm)	100.01	99.91
Uranium Oxide Mass (g)	115.11	115.24
Fuel Height (mm)	7.066	7.088
Packing Density (g/cc, % Th)	9.0, 82	9.0, 82
U Isotopic Abundances (wt %)	234-0.034, 235-4.907, 236-0.034, 238-95.026	

This section of the report presents the temperature data from the startup test, followed by a summary of the thermal data from the clad thermocouples for both capsules over the entire 6-month history of the test. In addition to the thermal data, the cover gas from the two irradiation capsules was periodically sampled and counted for identification of fission gas concentrations. The primary purpose of this sampling was to provide a means of determining if large defects developed in the fuel cladding. The terms of the license amendment permitting irradiation of test fuel at the MITR-II required that fuel be double encapsulated. Cover gas sampling and comparison to baseline results was used to ensure that clad integrity was maintained.

14.2.1 Summary of Startup Test Thermal Data

Figure 14.11 is a plot of the inner and outer clad temperatures versus time for the startup test, while Figure 14.12 plots the aluminum surface temperatures over the same time period. The test consisted of a series of reactor power increases as indicated by the “MW” numbers on the graph. Full power for the MITR-II is nominally 5 MW and all of the limiting safety calculations were made for that power, although in practice the maximum power level at which the reactor operates is 4.8 MW. Power was limited to 4.5

MW during the startup test. While the thermal safety limits that were established for the experiment (less than 400 °C fuel clad temperature and wetted aluminum surface temperatures below the saturation temperature) were clearly met, there were several unexpected observations that needed to be analyzed before proceeding with the longer term irradiation of both irradiation capsules. First and most evidently, there was a large temperature asymmetry between the inner and outer clad surfaces. Second, the aluminum surface temperatures measured were well below the expected values, running within several degrees of the reactor bulk coolant temperature as measured by the reactor instrumentation. (Note that the rapid reduction in aluminum surface temperatures seen during the reactor power increase is due to a change in the reactor secondary cooling alignment that resulted in a reduction in bulk reactor coolant temperature.) It can also be observed that the thermal equilibration time for the irradiation capsules is short, on the order of a minute, as is expected due to the high power density and small size of the capsules. One other feature of the temperature behavior that can be seen in this data is the slight reduction in outer clad temperature and increase in inner clad temperature indicated by the arrow labeled “sampling”. This behavior is associated with the cover gas pressure change that occurs during cover gas sampling and was fairly consistently observed over the course of the run.

The analysis and conclusions from this data are discussed more fully in the section on asymmetries following the summary of the longer-term run data. Briefly, however, the asymmetry must arise from differences in the effective gap conductivity between the inner and outer gaps. We believe that this is associated primarily with the behavior of the solid/solid interface between the LBE and the cooled aluminum sample walls.

Figure 14.11. Clad temperature history for the annular fuel startup test.

Figure 14.12. Aluminum capsule surface temperature history for the annular fuel startup test.

14.2.2 Summary of 2-Capsule Run Thermal Data

Figure 14.13 summarizes all the thermal data from the two capsules for the entire run. The capsules according to their in-core position, starting at the bottom of the core. Thus, Capsule 3 is immediately above the core axial centerline, while Capsule 4 is the uppermost, and lowest flux, position. The run has been split into 3 segments of 60 days each for clarity and each capsule is plotted individually, with the inner and outer clad temperature traces marked. A number of general observations can be made about the data. First, the temperature asymmetry between the inner and outer clad surfaces was clearly observed on both samples during the initial part of the run. The asymmetry was greatest immediately after a reactor startup with the temperatures tending to come together over periods of operation at constant reactor power. For Capsule 3, this behavior continued until the long reactor shutdown during the second half of May. For Capsule 4, however, after a brief shutdown on April 10, there was a change in behavior and the asymmetry showed little tendency to decrease with operation at constant power. Following the long May shutdown, both capsules showed a marked change in behavior, with the outer temperature predominantly higher than the inner temperature, although this was more consistently observed for Capsule 3. In fact, Capsule 3 continued to exhibit this temperature profile, at reasonably constant temperatures, for the remainder of the irradiation and largely independent of reactor power history. Capsule 4 was much more erratic in behavior, with the temperature asymmetry switching both sign and magnitude after reactor shutdowns. Capsule 4 also occasionally reverted to the original startup behavior, with a tendency for high initial inner clad temperature followed by a gradual trend toward equalizing the inner and outer temperatures, although the equalization was

not as marked as in the early part of the test. As briefly mentioned above, we attribute these changes in the temperature profile to changes in the effective gap thermal conductivity. Although we were not able to quantitatively model these effects or fully explain the evolution of the sample temperature behavior, we did perform thermal analyses of the behavior as discussed below.

Capsule 4

Capsule 3

Capsule 4

Capsule 3

Capsule 4

Figure 14.13. Temperature history for the annular fuel irradiation run.

14.2.3 Temperature Asymmetry Analysis and Discussion

Because the sample fuel is vibration-packed powder (VIPAC), there are no fuel-clad gaps. The geometry of the irradiation capsules (i.e. the ratio of inner and outer gap thicknesses and heat transfer areas) was chosen to produce a symmetric temperature distribution on the assumption that the effective conductivity of the LBE gap is the same for both inner and outer gaps. It is therefore reasonable to conclude that the observed asymmetries arise from differences in these effective conductivities. Recalling that the LBE is liquid where it contacts the fuel clad but solid where it contacts the aluminum capsule wall, the overall thermal conductivity can be seen to be a result of the clad/LBE contact resistance, the liquid LBE conductivity, the liquid/solid LBE resistance, the solid LBE conductivity, the solid LBE/aluminum contact resistance, the aluminum conductivity and the water-side effective heat transfer coefficient. Of these, the solid LBE/aluminum contact resistance is the most likely to depend on reactor power history and to differ in the inner and outer gaps. For example, consider the effect of thermal expansion as the capsule is heated. Because the aluminum surfaces are effectively cooled by the reactor coolant water, they expand relatively little. The fuel and solid LBE shells, however, will increase their diameter. This should increase the contact pressure between the solid LBE and the aluminum in the outer gap, while reducing it in the inner gap and could explain the initial trend to higher inner clad temperatures on startup. Creep of the solid LBE at temperature might explain the reduction in this effect over a period of

constant power operation. Although we consider this explanation plausible, we have not quantitatively validated it. Nor can we extend this argument to explain why the behavior of the samples changed over the course of the run and why the two capsules behaved differently. It is important to remember that while this is a technologically interesting problem and is relevant to planning for any future irradiation of a similar type in the MITR-II, it arises solely from the requirement to use a passive heat rejection mechanism in this reactor test and is not important to eventual application of IXAF fuel in forced circulation water environments.

An alternative explanation for the observed temperature asymmetry is that there were inconsistencies in the thermocouple attachment to the fuel cladding. Because the thermal gradients in the LBE gaps are large, small differences in placement could result in relatively large differences between the measured temperature and the actual surface temperature. However, the consistency between the temperatures observed for the two different samples when irradiated under similar conditions argues against this view. Furthermore, there is no likely mechanism to change this type of error during operation at constant power as was observed during the early part of the run.

From the point of view of carrying out the irradiation program at the MITR-II, the most important consequence of the temperature asymmetry was its relevance to the calculation of aluminum surface temperatures which was necessary to ensure that ONB was avoided in the experiment. This was of particular importance because of the apparent problem with measuring the aluminum surface temperature noted above. Space and material constraints associated with performing the in-core experiment dictated the attachment method for the aluminum surface thermocouples, however, it was clear from the results that this resulted in temperature measurements closer to the bulk coolant temperature than the wall temperature. Figure 14.14 compares the results of a TAFIX code calculation for the single capsule test, with effective gap conductance set to 10 W/m-K for the outer gap and 3.9 W/m-K for the inner gap. It is clearly possible to reproduce the measured clad temperatures by making this adjustment, and this calculation shows the fuel temperature profile that would be expected. Note also that the measured aluminum surface temperatures are considerably below what would be expected for the resultant heat flux and coolant flow parameters. In response to this measurement uncertainty, and the consequent absence of a way to independently verify the MCNP-calculated heat generation rates, additional conservatism was applied to the thermal/hydraulic calculations and a set of permissible operating conditions was mapped in a region where there was high confidence that aluminum surface temperatures would remain below the saturation temperature.

Figure 14.14. Comparison of calculated and experimental temperature values for the case of asymmetrical gap conductances at 4 MW. The experimental points are measured clad and aluminum surface temperatures.

14.3. Post Irradiation Examination Results

14.3.1 Overview of Gamma Scan Data

Following the completion of the irradiation the two sample capsules were stored in the reactor core tank wet storage ring for decay to levels at which they could be handled. The samples were then transferred to the reactor spent fuel storage pool and were brought one at a time to a hot box for gamma scanning using an intrinsic Ge detector and a shielded collimator as shown in Figure 14.15. Approximately 21 nuclides were identified in the gamma scans and their axial variation was mapped. Figures 16 and 17 show the results for nuclides associated with fuel and cladding activities, respectively, for Capsule 3. Similar results were obtained for Capsule 4. Burnup and fission gas release estimates based on the counting results for both capsules are presented in the following section.

Figure 14.15. Solid model of the shielded collimator and photograph of an irradiation capsule in position for counting in the hot cell.

Figure 14.16. Plot of fuel activities versus axial position in the Capsule 3. Position relative to the fuel structures is indicated by the schematic illustration showing the clad, ceramic spacers, fuel and spring in the fuel assembly.

Figure 14.17. Plot of cladding activities as a function of axial position in Capsule 3. Position relative to the fuel structures is indicated by schematic illustration showing the clad, ceramic spacers, fuel and spring in the fuel assembly.

14.3.2 Burnup Estimates From Calculation and Gamma Scan Data

The burnup of the annular fuel capsules was calculated using both computer codes and experimental measurements. We have high confidence in the burnup calculated by MONTEBURNS [Poston and Trelue, 1999] which gives 7.32 MWd/kg for Capsule 3, and 5.64 MWd/kg for Capsule 4. MONTEBURNS couples MCNP code [Briesmeister, 2000], which calculates cross sections of actinides and fission products and updates 1-group libraries, and ORIGEN2.2 [Croff, 1980], which calculates isotopics changes using the updated libraries. The MCNP model used for the MITR-II core has been extensively benchmarked and the reactivity worth predictions for the annular fuel experiment were within 20% of the measured values. Although precise experimental verification from the gamma scans described above is difficult, we used a number of approaches in order to both provide additional support for the calculated burnup and to form an estimate of the accuracy of the gamma scan data which is also used to make a bounding estimate of the fission gas release.

The most reliable experimentally based methods to calculate burnup is based on the ratio of Cs-134 to Cs-137 count rates. This method was advantageous because it does not require an absolute activity calibration of the detector, however it does use energy-averaged fission and capture cross sections. A relation involving the ratio of the count

rates for low burnup in a thermal spectrum was given by Zhengpei and Wenfeng [2002] as,

$$\frac{R_4}{R_7} = \frac{\lambda_4 \cdot Y_3 \cdot RI_4}{2 \cdot \lambda_7 \cdot Y_7 \cdot RI_7} \cdot \sigma_3 \cdot \int \varphi(t) \cdot dt \quad (1)$$

where R is the cesium count rate, λ the decay constant, RI the relative intensity of the measured gamma ray, and σ_3 the absorption cross section for Cs-133. This was then combined with the relation for the burnup to eliminate the fluence dependence,

$$B = \frac{1}{\rho} \cdot E_f \cdot \Sigma_f \cdot \int \varphi(t) \cdot dt \quad (2)$$

where ρ is the fuel density, E_f is the energy released per fission and Σ_f is the fission macroscopic cross section. All constants except the cross sections were obtained from the KERI Table of the Nuclides [Chang, 2005], which is based on the JENDL and ENDF libraries. The cross sections depend on the spectrum at the sample position in the MITR core, and were therefore obtained from the MCNP model results. Data used in the calculations are summarized in Table 14.3.

Table 14.3 Listing of data used in burnup calculations.

Variable	Value	Description
λ_7	7.31×10^{-10} (1/s)	Cs-137 decay constant ($t_{1/2} = 30.07$ yr)
λ_4	1.06×10^{-8} (1/s)	Cs-134 decay constant ($t_{1/2} = 2.065$ yr)
Y_7	6.27×10^{-2}	Cs-137 accumulated fission yield
Y_3	6.70×10^{-2}	Cs-133 accumulated fission yield
E_f	0.365×10^{-21} (MWd)	Energy released per fission (197 MeV)
Σ_f	capsule 3 = 0.073 (1/cm) capsule 4 = 0.079 (1/cm)	Fission cross section
RI_4	0.976	Relative intensity of Cs-134 605 keV gamma
RI_7	0.851	Relative intensity of Cs-137 662 keV gamma
σ_3	capsule 3 = 12.3×10^{-24} (cm ²) capsule 4 = 12.4×10^{-24} (cm ²)	Cs-133 neutron absorption cross section
ρ	7.93 (g/cm ³)	Uranium density

Using this ratio method with data from Table 14.3, the burnup was calculated within 6% of the MONTEBURNS results. The accuracy of this experimental burnup measurement was evaluated by using ORIGEN2.2-derived isotope concentrations. Because ORIGEN2.2 used the MCNP-generated cross section libraries, it was expected the burnup estimate would be very close to the MONTEBURNS result. Figure 14.18 shows the measured ratio of Cs-134/Cs-137 for the two capsules, while Table 14.4 gives an axially averaged experimental value and the values from ORIGEN2.2.

Figure 14.18. Ratio of the Cs-134 to the Cs-137 net count rate in the fuel region at the end of irradiation based on experimental measurements.

Table 14.4 Computed ratio of Cs-134 to Cs-137 net count rates at the end of irradiation.

Source	Cs-134/137 Ratio	
	Capsule 3	Capsule 4
Measured	0.225	0.174
ORIGEN	0.211	0.146

ORIGEN2.2 calculations of Cs-134 and Cs137 concentrations were made by simulating the irradiation of annular fuel for the appropriate number of effective full power days and decaying the fuel until the time of measurement. Irradiation calculations were made using 1-group cross section libraries generated by MONTEBURNS at the midpoint of irradiation. The cross sections were kept constant during ORIGEN2.2 depletions. This is a good assumption since the spectrum at a capsule position is not affected by annual fuel depletion, but by MITR core fuel, since most neutrons supplied to annular fuel come from the highly enriched MITR fuel fissions.

Table 14.5 summarizes various burnup estimates. While the agreement between MONTEBURNS predictions and experimental results is very good (within 6%), the Cesium ratios generated by ORIGEN2.2 yield 20% difference from MONTEBURNS burnups. The reason for this discrepancy comes from differences in cross sections of parent nuclides of these cesium isotopes, which were not tracked by MONTEBURNS, because of their small effect on neutronic reactivity, and thus did not reflect MITR-spectrum updated cross sections (PWR cross section library was used as a basic library, which has significantly different spectrum than the capsule location in the MITR core).

Table 14.5 Calculated values of burnup from various methods.

Source	Burnup (MWd/kg)	
	Capsule 3	Capsule 4
MONTEBURNS	7.32	5.64
Measured Cs-134/Cs-137 ratio	6.91	5.74
ORIGEN (Cs-134/Cs-137 ratio)	6.48	4.82

14.3.3 Fission gas release estimates

14.3.3.1 Fission gas release predictions

The fission gas release for the fuel samples was first predicted with FRAPCON-ANNULAR (VIPAC) discussed in Chapter 13. The fuel sample is composed of three fractions with smear density of 87.5%. Since the code was unable to model VIPAC fuel with mixed particle sizes, the FGR was predicted by calculating FGRs for a single particle size for 3 different sizes and the final result was obtained by weight fraction. The predicted fission gas release for different burnups is shown in Figure 14.19. For burnups of 7.32MWd/kg and 5.64MWd/kg for capsule 3 and 4, FRAPCON-ANNULAR predicts fission gas release of 0.7 and 0.6%.

Figure 14.19 Fission Gas Release Predictions for Fuel Irradiation Test

14.3.3.1 Fission gas release measurements

Fission gas release estimates were made based on the results of the collimated gamma scan data using Krypton-85 activity with a principal gamma energy of 514 keV. The Kr-85 count rate at 514 keV, per position, is presented in Figure 14.20. Because this peak was not distinguishable from the 511 keV peak caused by annihilation photons, there is some uncertainty in the magnitude. Although there were not a large number of high-energy gamma rays (able to cause pair production) detected, there were up to several thousand counts per second above 1 MeV, presumably from activation of the spring and other capsule components near the plenum. Possibility to use Cs-137, which is the product of Xe-137 decay was also explored, but it was not found to be a reliable measure of fission gas release because only a small amount reaches the plenum since the half-life of the gaseous Xe-137 is only a few minutes, and the cesium daughter is an immobile solid.

Figure 14.20. Net Krypton-85 counts per second across both capsules

Because it has a relatively long half life of 10 years, and there is no manufactured gap, it was assumed that all Kr-85 that was released migrated to the plenum. Because the count rate is proportional to the atomic abundance, the fission gas release percentage can be found using count rates and the length of the fuel and plenum,

$$\text{FGR} = \frac{l_p \cdot \overline{c_p}}{l_p \cdot \overline{c_p} + l_f \cdot \overline{c_f}} \quad (3)$$

where l_p is plenum length, l_f is fuel length, and the average count rates for the plenum and fuel are $\overline{c_p}$ and $\overline{c_f}$, respectively.

Because of the long Kr-85 half life, it is probable that the released gas has migrated into the plenum in the year since irradiation ended. This assumption gives values of fission gas release between 0.5% and 0.7% for both capsules. Given the uncertainty over interfering annihilation photons, this is probably an upper bound estimate. Although, both the prediction and measurements carry significant uncertainty, both show small fission gas release for VIPAC fuel specimens not exceeding 1%. This is not surprising given a small burnups achieved, but it tends to lend some support for the modeling effort.

14.4. Summary and Conclusions

Two samples of Zircaloy clad VIPAC UO₂ IXAF fuel were irradiated for a period of approximately six months in the MITR-II research reactor. Power density was representative of the highest power pin in a core operating at 50% higher power than current generation PWRs. The temperature profile observed had varying asymmetry between the inner and outer clad temperatures, but was in general somewhat lower than projected for the maximum in commercial fuel. Burnups achieved for the two samples were approximately 7.1 and 5.7 MWd/kg. Although only preliminary post-irradiation examination has been completed, the burnup calculations based on MONTEBURNS have been generally corroborated. A fission gas release fraction of less than 1% has also been calculated based on the preliminary PIE using Krypton-85 activity, which agreed well with FRAPCON-ANNULAR (VIPAC) predictions. However, both the prediction and measurement data carry high uncertainty. No unforeseen problems with IXAF fuel irradiation behavior were identified in these experiments. More extensive post-irradiation examination of the fuel would be needed to validate the predictions on the entire range of expected burnup in commercial reactors.

15. CONCLUSIONS

A new high-power density PWR fuel of annular shape, with two claddings, and cooled from both the inside and outside surfaces has been proposed and its performance has been evaluated including manufacturing issues. The following conclusions can be made:

1. Annular fuel exhibits much lower average fuel temperature (by 600°C) and peak fuel temperature (by about 1500°C) than a typical solid fuel rod at fixed assembly power. The remarkably low peak fuel temperature remains even after significant power increase, for example operation at 150% power density results in a peak fuel temperature 1300°C lower than that of a solid rod at 100% power. The average fuel temperature is 400°C lower.
2. One benefit of such a low fuel temperature is low stored energy and improved LOCA performance. RELAP5/MOD3 analysis shows that the blowdown peak cladding temperature (PCT) is eliminated and the PCT exhibits also larger margin during the core heat up phase for the same power rating. Moreover, the PCT during the heatup phase remains below the 1200°C limit even for uprated core at 150% power rating.
3. Another advantage of the low fuel temperature is reduced fission gas release allowing achievement of high burnups. FRAPCON-ANNULAR results show that the fission gas release from the annular fuel remains below 6% at ~85MWd/kg.
4. A properly designed annular fuel exhibits higher MDNBR margin than solid rods at the same power density. At the same MDNBR margin, the 13x13 array was found to provide the best performance allowing 50% power density increase, provided that the flow rate is increased proportionally. However, 50% higher flow rate would require pumps that may be beyond currently available experience base. Hence, lower power uprate or reduced core inlet temperature may be needed. The optimum power uprate level and core inlet temperature selection has to be derived from plant economic analyses.
5. The MDNBR results carry uncertainties because the heat fluxes to the inner and outer channels depend on the gap conductances, which are difficult to predict without experimental data. Therefore, irradiation tests are needed to benchmark the fuel performance code and MDNBR predictions. Large gap conductance asymmetries can be substantially mitigated by using a smaller inner gap in the cold state or by sputtering of ZrO₂ on the outer pellet surface. VIPAC fuel does not have any initial gaps and does not pose an asymmetry problem.
6. Introduction of annular fuel with internal and external cooling has only small effect on reactor physics characteristics since the moderator to heavy metal ratio is preserved. The additional cladding results in small parasitic losses and the inner

moderator increases neutron capture in U238, which requires a slight increase in initial enrichment unless other measures, such as use of beryllium rods or high (TD 96%) density fuels, are employed. The 150% power density annular fueled core that maintains the current 18-month cycle length generates 50% more energy and requires the enrichment to increase to 8-9%. Detailed neutronic analyses and core management studies confirmed the feasibility of 150% core, although more burnable poison than for the reference solid fuel is needed to limit peaking and soluble boron concentration.

7. Annular fuel pellets can be manufactured via the traditional press and sinter route in good yield and to the required dimensional specifications. Achieving tighter dimensional tolerances on the pellet ID surface that are needed to achieve a balanced heat split was also demonstrated.
8. Based on tests performed at AECL, it appears possible that annular fuel elements be made via the VIPAC manufacturing process, but only to smear densities of 77% as compared to the needed 85%. Addition of elemental uranium powder, as has been demonstrated in some Russian LWR solid fuel tests, can increase the effective smear density to the required 85% or even above.
9. The screening irradiation of VIPAC annular fuel confirmed that predicted burnups were within several percent and showed lower than predicted fission gas release. Fission gas release (FGR) was evaluated by comparing the integrated counts for the Kr-85 514 keV peak in the fuel to the integrated counts in the plenum region, but these values are subject to fairly large uncertainty because the 514 keV peak is close to the 511 keV annihilation peak and is not fully resolved from it. These results show that at burnup of 7.3MWd/kg, FGR is below 1% per cent of the fission gas generated. Similar FGR was also predicted by FRAPCON-ANNULAR (VIPAC) code.
10. High power density core with annular fuel are economically more attractive than solid fuel core, especially for future PWRs, where up to 11% rate of return on investment can be expected. The benefit to existing reactors depends on the estimated loss of revenue during plant uprate, and the remaining lifetime of the plant.

In conclusion, the proposed internally and externally cooled annular fuel appears to be a very promising candidate for high power density PWRs and deserves further investigations. In particular, the irradiation of a rod with pellet fuel in test reactor and of lead rods in power reactors is desirable next steps.

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APPENDIX B - COST ESTIMATION STUDIES FOR GENERATION III NUCLEAR PLANTS

This appendix summarizes the cost data from a study performed by the Westinghouse Electric Company and the Electric Power Research Institute (EPRI), funded by the Department of Energy (Contract number DE-FC07-03ID14535). This data was used to estimate the 600 MWe uprates (Table B-1) and the 1,117 MWe and 1,717 MWe plants (Table B-2) and provided the basis for the models:

$$\text{Installed Capital Cost (\$/KW(e))} = 10^{(4.06-0.309*\text{LOG(Power (MWe))})}$$

The adjustments are for a smaller containment (assumed to be 10% smaller) and reactor vessel and internals (assumed to be 25% smaller) than for the standard fuel versions. The cost models for these two components are:

$$\text{Reduction in Installed Containment Cost (\$/KW(e))} = 0.1 * 10^{(-0.01-0.6598*\text{LOG(Power (MWe))})}$$

$$\text{Installed Reactor Vessel Cost (\$/KW(e))} = 0.75 * 10^{(-1.1-0.9992*\text{LOG(Power (MWe))})}$$

The data and fits are provided in Table B-3.

Table B-1 - Westinghouse Estimates of 25 % and 50 % Ultra Uprate Costs

Account	Ultra Uprate Power (MW(e))	300	600
	Ultra Uprate Level	25%	50%
211 Yardwork	\$	1,113,542.15	\$ 2,227,084
212 Reactor Containment Building	\$	-	\$ -
213 Turbine-Generator Bldg	\$	12,521,896.95	\$ 19,785,632
215 Annex & Auxiliary Building	\$	-	\$ -
215A Annex Building	\$	-	\$ -
215B Auxiliary Building	\$	-	\$ -
216 Radwaste Building	\$	-	\$ 444,841
218A Diesel Generator Building	\$	-	\$ -
Containment hatch mods	\$	15,000,000.00	\$ 30,000,000
21 Structures & Improvements	\$	28,635,439.10	\$ 52,457,557
221 Reactor Equipment	\$	-	\$ -
222 Main Heat Transfer System	\$	62,455,680.55	\$ 183,370,078
223 Passive Safety System	\$	-	\$ -
224 Radwaste Processing Sys.	\$	-	\$ 7,196,862
225 Fuel Handling and Storage	\$	6,593,151.69	\$ 13,186,303
226 Reactor Auxiliary Systems	\$	-	\$ 7,446,068
227 Instrumentation & Control	\$	-	\$ 61,723,619
228 Reactor Plant Miscellaneous Items	\$	7,757,334.98	\$ 12,257,230
22 Reactor Plant Equipment	\$	76,806,167.23	\$ 285,180,160
231 Turbine-Generator Equip.	\$	47,584,074.24	\$ 81,708,083
233 Condensing Systems	\$	14,715,091.46	\$ 23,090,454
234 Feedwater System	\$	6,575,763.49	\$ 9,966,994
235 Other Turbine Plant Sys.	\$	1,715,930.46	\$ 2,711,312
236 Turbine Plant I&C	\$	6,310.27	\$ 9,980
237 Turbine Plant Miscellaneous Items	\$	6,009,379.31	\$ 9,495,316
23 Turbine Plant Equipment	\$	76,606,549.22	\$ 126,982,138
242 EPE Station Service	\$	18,323,891.65	\$ 24,011,507
244 Protective Equipment	\$	7,961,884.05	\$ 10,433,200
24 Electric Plant Equipment	\$	26,285,775.70	\$ 34,444,707

Table B-1 (Cont.) - Westinghouse Estimates of 25 % and 50 % Ultra Uprate Costs

Account	Uprate Power (MW(e))	300	600
	Ultra Uprate Level	25%	50%
251 Transportation & Lifting Equip.	\$	-	\$ 11,304,906
252 Air & Water System	\$	-	\$ -
253 Communication System	\$	-	\$ -
254 Furn, Fixtures & Lab Equip	\$	-	\$ -
255 Waste Water Treatment Equip	\$	1,284,096.98	\$ 1,475,040
25 Miscellaneous Plant Equip.	\$	1,284,096.98	\$ 12,779,946
261 Heat Rejection Sys. Struct.	\$	24,960,943.65	\$ 32,936,163
262 Heat Rejection Sys. Equip.	\$	1,330,164.78	\$ 1,755,163
26 Main Cond. Heat Reject Sys	\$	26,291,108.43	\$ 34,691,326
2 Total Direct Costs Summary	\$	235,909,136.66	\$ 546,535,834
Non Allocated Equipment Shipping Co	\$	4,957,483.27	\$ 8,053,455
Modularization Cost Reduction	\$	-	\$ -
Premium Time Labor Adder	\$	9,413,151.43	\$ 15,291,710
Unscheduled Direct Overtime Labor A	\$	4,235,918.24	\$ 6,881,269
2 Total Direct Costs	\$	254,515,689.60	\$ 576,762,268
\$ or MH Per Net KWe	\$	848.39	\$ 961
911 Temporary Construction Facilities	\$	23,383,455.51	\$ 37,986,536
912 Construction Tools & Equipment	\$	23,537,232.15	\$ 38,236,346
913 Payroll Insurance & Taxes	\$	17,362,836.76	\$ 28,206,012
914 Permits, Insurance & Local Taxes	\$	1,490,186.18	\$ 2,420,815
91 Construction Services	\$	65,773,710.60	\$ 106,849,708
921 Home Office Engineering	\$	30,141,300.05	\$ 48,964,686
922 Home Office QA/QC	\$	978,125.69	\$ 1,588,970
923 Home Office Construction Managemen	\$	3,399,760.32	\$ 5,522,927
92 Eng. & Home Office Services.	\$	34,519,186.06	\$ 56,076,583
931 Field Office Expenses	\$	2,399,060.70	\$ 3,897,286
932 Field Job Supervision	\$	32,542,096.62	\$ 52,864,792
933 Field QA/QC	\$	5,980,143.04	\$ 9,714,771
934 Plant Start-up & Testing	\$	3,652,974.27	\$ 5,934,274
93 Field Sup & Field Off Services	\$	44,574,274.63	\$ 72,411,123

Table B-1 (Cont.) - Westinghouse Estimates of 25 % and 50 % Ultra Uprate Costs

Account	Ultra Uprate Level	25%	50%
Direct Labor Premium Time Pay Taxes	\$	720,106.37	\$ 1,169,816
Construction Labor Premium Time Labor A	\$	2,269,591.33	\$ 3,686,962
Unscheduled Indirect Overtime Labor Add	\$	948,737.72	\$ 1,541,229
9 TOTAL INDIRECT COSTS	\$	148,805,606.72	\$ 241,735,421
\$ Per Net KWe	\$	496.02	\$ 403
2+9 BASE CONSTRUCTION COSTS (TPC)	\$	403,321,296.32	\$ 818,497,690
\$ Per Net KWe	\$	1,344.40	\$ 1,364
941 Management & Integration	\$	4,533,287.39	\$ 7,364,347
942 Taxes & Insurance	\$	3,307,131.15	\$ 6,706,971
943 Spare Parts & Initial Supplies	\$	6,156,079.91	\$ 12,312,160
944 Staff Training & Startup	\$	32,456,850.03	\$ 52,726,308
945 General & Administrative	\$	5,548,520.61	\$ 9,013,598
946 Capital Equipment	\$	9,288,638.56	\$ 18,577,277
Allocated Non-Recurring	\$	2,296,641.40	\$ 4,593,283
Land and Land Rights	\$	2,551,823.78	\$ 5,103,648
94 Owner's Cost	\$	290,832,972.83	\$ 116,397,592
TOTAL OVERNIGHT COSTS	\$	694,154,269	\$ 934,895,281
\$ Per Net KWe	\$	2,314	\$ 1,558
Normalized uprate Overnight Cost	\$	2,050	\$ 1,381
Replacement Power	\$	224,694,000	\$ 224,694,000
Fuel Cost	\$	71,000,000	\$ 71,000,000
Total Overnigt Costs w/RP	\$	989,848,269	\$ 1,230,589,281
\$ Per Net KWe	\$	3,299	\$ 2,051
\$Per Net KWE normalized to Ap1117'	\$	2,923	\$ 1,817

**Table B-2 - Comparisons of New Plant Construction Costs
for AP1117, 600 MWE, 1200 MWE, 1800 MWE**

Acc	Description	AP1117	600.00	1200	1800
211	Yardwork	8,292,177.21	4,454,168.60	8,908,337.20	13,362,505.79
212	Reactor Containment Building	100,214,622.69	66,496,046.72	105,069,247.97	137,308,004.25
213	Turbine-Generator Bldg	29,818,458.73	19,785,631.79	31,262,932.99	40,855,445.52
215	Annex & Auxiliary Building	4,396,509.16	2,917,243.72	4,609,486.11	6,023,830.47
215A	Annex Building	15,896,138.99	10,547,666.33	16,666,184.28	21,779,926.54
215B	Auxiliary Building	47,898,259.57	31,782,237.17	50,218,560.69	65,627,293.22
216	Radwaste Building	3,352,046.97	2,224,205.07	3,514,427.79	4,592,771.67
218A	Diesel Generator Building	1,878,110.26	1,246,194.46	1,969,090.22	2,573,272.89
21	Structures & Improvements	211,746,323.58	139,453,393.85	222,218,267.24	292,123,050.36
221	Reactor Equipment	88,161,146.61	47,356,032.20	94,712,064.40	142,068,096.60
222	Main Heat Transfer System	176,696,272.01	117,975,312.65	185,123,108.50	240,946,128.52
223	Passive Safety System	56,008,423.91	37,163,626.15	58,721,599.92	76,739,349.02
224	Radwaste Processing Sys.	10,846,221.21	7,196,862.22	11,371,636.94	14,860,835.15
225	Fuel Handling and Storage	24,548,501.46	13,186,303.38	26,372,606.76	39,558,910.15
226	Reactor Auxiliary Systems	10,811,056.98	7,446,068.08	11,286,128.74	14,394,605.11
227	Instrumentation & Control	101,508,839.47	66,731,940.65	106,600,823.82	140,934,999.14
228	Reactor Plant Miscellaneous Items	18,472,582.38	12,257,230.21	19,367,436.48	25,310,013.17
22	Reactor Plant Equipment	487,053,044.03	309,313,375.55	513,555,405.57	694,812,936.87
231	Turbine-Generator Equip.	132,674,824.17	81,708,082.93	140,303,471.76	192,495,064.88
233	Condensing Systems	34,583,481.61	23,090,453.52	36,232,805.28	47,158,640.70
234	Feedwater System	14,471,226.31	9,966,993.66	15,107,137.40	19,268,013.18
235	Other Turbine Plant Sys.	4,086,154.18	2,711,311.90	4,284,096.82	5,598,600.88
236	Turbine Plant I&C	15,078.17	9,980.02	15,814.68	20,719.90
237	Turbine Plant Miscellaneous Items	14,310,166.39	9,495,315.83	15,003,383.54	19,606,922.97
23	Turbine Plant Equipment	200,140,930.83	126,982,137.85	210,946,709.49	284,147,962.51
242	EPE Station Service	30,597,163.22	24,011,506.75	31,464,520.06	36,855,024.37
244	Protective Equipment	13,294,723.12	10,433,200.35	13,671,596.91	16,013,816.09
24	Electric Plant Equipment	43,891,886.34	34,444,707.09	45,136,116.97	52,868,840.46

**Table B-2 (continued) - Comparisons of New Plant Construction Costs
for AP1117, 600 MWE, 1200 MWE, 1800 MWE**

Acc	Description	AP1117	600.00	1200	1800
251	Transportation & Lifting Equip.	12,801,101.19	11,304,905.76	12,985,926.65	14,082,870.87
252	Air & Water System	30,537,576.82	26,968,338.33	30,978,485.87	33,595,293.45
253	Communication System	3,359,616.35	2,966,943.68	3,408,123.32	3,696,013.53
254	Furn, Fixtures & Lab Equip	2,858,827.84	2,524,687.44	2,900,104.30	3,145,081.25
255	Waste Water Treatment Equip	1,670,260.49	1,475,040.09	1,694,376.12	1,837,503.07
25	Miscellaneous Plant Equip.	51,227,382.70	45,239,915.28	51,967,016.27	56,356,762.17
261	Heat Rejection Sys. Struct.	42,231,233.56	32,936,162.61	43,459,527.11	51,111,838.16
262	Heat Rejection Sys. Equip.	2,250,495.82	1,755,162.95	2,315,951.39	2,723,741.85
26	Main Cond. Heat Reject Sys	44,481,729.38	34,691,325.55	45,775,478.50	53,835,580.01
2	Total Direct Costs Summary	1,038,541,296.86	690,124,855.18	1,089,598,994.04	1,434,145,132.37
	Non Allocated Equipment Shippin	12,442,670.49	8,053,455.33	13,082,876.78	17,376,693.16
	Modularization Cost Reduction	-	-	-	-
	Premium Time Labor Adder	23,625,847.06	15,291,709.62	24,841,455.56	32,994,452.07
	Unscheduled Direct Overtime Lab	10,631,631.41	6,881,269.48	11,178,655.25	14,847,503.77
2	Total Direct Costs	1,085,241,445.81	720,351,289.61	1,138,701,981.63	1,499,363,781.38
	\$ or MH Per Net KWe	971.57	1,200.59	948.92	832.98
911	Temporary Construction Facilities	58,689,584.21	37,986,535.55	61,709,309.06	81,962,380.81
912	Construction Tools & Equipment	59,075,544.58	38,236,346.44	62,115,128.05	82,501,390.09
913	Payroll Insurance & Taxes	43,578,575.00	28,206,011.53	45,820,800.91	60,859,244.58
914	Permits, Insurance & Local Taxes	3,740,183.19	2,420,814.59	3,932,624.90	5,223,317.27
91	Construction Services	165,083,886.98	106,849,708.11	173,577,862.92	230,546,332.75
921	Home Office Engineering	75,650,939.03	48,964,686.38	79,543,367.70	105,649,599.61
922	Home Office QA/QC	2,454,974.64	1,588,969.88	2,581,289.18	3,428,471.49
923	Home Office Construction Manage	8,532,978.35	5,522,926.94	8,972,021.28	11,916,649.79
92	Eng. & Home Office Services.	86,638,892.02	56,076,583.20	91,096,678.17	120,994,720.90
931	Field Office Expenses	6,021,345.94	3,897,285.60	6,331,159.14	8,409,053.42
932	Field Job Supervision	81,676,641.80	52,864,791.92	85,879,107.84	114,064,737.52
933	Field QA/QC	15,009,420.17	9,714,771.04	15,781,692.11	20,961,263.03
934	Plant Start-up & Testing	9,168,514.07	5,934,274.21	9,640,256.89	12,804,201.16
93	Field Sup & Field Off Services	111,875,921.98	72,411,122.77	117,632,215.99	156,239,255.13
9	Total Indirect Cost Summary	363,598,700.98	235,337,414.08	382,306,757.07	507,780,308.78
	Direct Labor Premium Time Pay T	1,807,378.01	1,169,816.24	1,900,372.09	2,524,076.57
	Construction Labor Premium Time	5,696,393.83	3,686,962.00	5,989,487.44	7,955,244.64
	Unscheduled Indirect Overtime La	2,381,214.46	1,541,228.98	2,503,733.86	3,325,462.41
9	TOTAL INDIRECT COSTS	373,483,687.28	241,735,421.29	392,700,350.46	521,585,092.41
	\$ Per Net KWe	334.36	402.89	327.25	289.77
2+9	BASE CONSTRUCTION COSTS	1,458,725,133.09	962,086,710.90	1,531,402,332.09	2,020,948,873.78
	\$ Per Net KWe	1,305.93	1,603.48	1,276.17	1,122.75
941	Management & Integration	11,377,991.25	7,364,347.10	11,963,417.16	15,889,825.50
942	Taxes & Insurance	11,977,485.87	7,829,854.26	12,588,190.70	16,722,723.21
943	Spare Parts & Initial Supplies	22,921,137.52	12,312,159.81	24,624,319.63	36,936,479.44
944	Staff Training & Startup	81,462,683.39	52,726,308.43	85,654,140.74	113,765,935.94
945	General & Administrative	13,926,101.20	9,013,598.33	14,642,633.68	19,448,364.22
946	Capital Equipment	34,584,697.55	18,577,277.11	37,154,554.22	55,731,831.33
	Allocated Non-Recurring	8,551,161.48	4,593,282.80	9,186,565.60	13,779,848.41
	Land and Land Rights	9,501,290.54	5,103,647.56	10,207,295.12	15,310,942.67
	Replacement Power	-	-	-	-
94	Owner's Cost	194,302,548.79	117,520,475.40	206,021,116.85	287,585,950.72
	TOTAL OVERNIGHT COSTS	1,653,027,681.89	1,079,607,186.30	1,737,423,448.95	2,308,534,824.51
	\$ Per Net KWe	1,479.88	1,799.35	1,447.85	1,282.52
	Normalized \$ per Net Kwe	1,310.58	1,593.50	1,282.22	1,135.80

**Table B-3 – Data and Fits for the total installed capital, reactor vessel and
containment costs versus output electrical power**

Total Capital Cost			
Output Power (MW(e))	Installed Overnight Cost (\$/kw)	Estimated Installed Overnight Cost (\$/kw)	
600	\$ 1,594	\$ 1,591	
1117	\$ 1,311	\$ 1,313	
1200	\$ 1,282	\$ 1,284	
1800	\$ 1,136	\$ 1,133	
1717		\$ 1,150	

Regression Statistics	
Multiple R	0.999878471
R Square	0.999756956
Adjusted R Square	0.999635435

	Coefficients	P-value
Intercept	4.059748955	6.52282E-06
log size	-0.308857938	0.000121529

Reduction in Reactor Containment Building Cost

Assumed reduction = 10%

Output Power (MW(e))	Installed Overnight Cost (\$/kw)	Estimated Installed Overnight Cost Reduction (\$/kw)	
600	\$ 67	\$ 7	
1117	\$ 100	\$ 10	
1200	\$ 105	\$ 11	
1800	\$ 137	\$ 14	
1717		\$ 13	

Regression Statistics	
Multiple R	0.9999991
R Square	0.999998201
Adjusted R Square	0.999996401

	Coefficients	P-value
Intercept	-0.010227532	0.168342923
2.77815125	0.659826166	0.000853974

Reduction in Reactor Vessel Cost

Assumed reduction = 25%

Output Power (MW(e))	Installed Overnight Cost (\$/kw)	Estimated Installed Overnight Cost Reduction (\$/kw)	
600	\$ 47	\$ 12	
1117	\$ 88	\$ 22	
1200	\$ 95	\$ 24	
1800	\$ 142	\$ 36	
1717		\$ 34	

Regression Statistics	
Multiple R	0.99999794
R Square	0.99999588
Adjusted R Square	0.99999382

	Coefficients	P-value
Intercept	-1.100391662	1.57412E-06
log size	0.999268087	2.05878E-07