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Title:

Evaluation of Core Compositions for Use in B&B Reactors and Limited-Separations Fuel Cycles

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ABSTRACT

Key Words: Breed-and-burn; limited separations fuel cycle; neutron excess

1. INTRODUCTION

A breed-and-burn (B&B) reactor is a reactor that can run on primarily fertile fuel (such as natural or depleted uranium) by first breeding it into usable fissile fuel. The newly-bred fuel is subsequently burned in the same reactor, which produces neutrons that can be used to breed additional fertile fuel. The primary challenge in designing B&B reactors is the fact that they require high levels of burnup and fluence to sustain critical operation, since feed fuel must be burned to a sufficient level for it to become a net producer of neutrons. The minimum burnup requirement of B&B reactors is discussed in [Petroski, 2010a], for the special case of “minimum-burnup” B&B reactors, i.e. reactors that are capable of shuffling fuel in three dimensions to achieve a uniform discharge burnup distribution. That paper introduces the infinite-medium depletion approximation as a method of quickly determining the required minimum burnup, and applies it to different reactor fuel compositions.

Subsequent work by the authors [Petroski, 2010b], defines the neutron excess concept, which is a measure of the net number of neutrons produced or absorbed by a material. The minimum burnup of a B&B core composition corresponds to the point at which its neutron excess first becomes positive; i.e. when the material becomes a net producer of neutrons. The neutron excess concept can be used to determine the amount of starter fuel required to establish a desired B&B equilibrium cycle [Petroski, 2011a]. Knowing the starter fuel requirement of a B&B reactor allows one to characterize fuel cycles linking B&B reactors, including computing their doubling times [Petroski, 2011b]. The Petroski, 2011b paper focused primarily on limited separations fuel cycles; i.e., fuel cycles that don’t use chemical actinide separation, and instead

use other recycling methods ranging from direct reuse to physical processes such as clad replacement or melt refining.

The goal of this study is to apply the methods developed in these previous studies of B&B reactors to broadly evaluate different core composition options for minimum-burnup B&B reactors. This study considers different possible options for fuel, structure, and coolant, and also considers the effect of different structure/fuel and coolant/fuel ratios. Also considered will be the effect of using natural vs. depleted uranium, the effect of fission gas removal from fuel, and the effect of using a melt refining process to refabricate used fuel.

The primary criteria for judging a given core composition are its minimum required burnup/fluence and its reactor doubling time. Minimum required burnup/fluence is tied to overall neutronic performance: core compositions with lower minimum burnup will have more reactive fuel that is capable of providing more net neutron excess. It is also extremely important for determining which combinations of materials are most promising for near-term implementation of B&B reactors, since high burnup and fluence are currently the limiting factors for B&B reactor design. Meanwhile, reactor doubling time depends on both neutronic performance and on achievable power density, so optimizing for doubling time requires considering the tradeoff between the neutronics and cooling ability of different coolants and different amounts of coolant. Doubling time is an important figure of merit since it determines the ability of a B&B reactor infrastructure to support growing energy demand, especially the growing demand for carbon-free energy.

2. MATERIALS AND METHODS

Two types of neutronic models are employed in this study. First, the infinite-medium depletion model described in [Petroski, 2010a] is used in an evaluation of the minimum burnup and material irradiation requirements of different core compositions. The second type is a one-dimensional infinite slab model of a B&B reactor, such as the one presented in [Petroski, 2011a]. This model consists of a set of infinite slabs that are shuffled repetitively and refueled with depleted uranium feed fuel until an equilibrium cycle is established.

The earlier work cited in the introduction shows how results from these simple models can be used to draw conclusions about three-dimensional B&B reactors. The neutron excess balance concept [Petroski, 2011a] is used to determine how much recycled fuel is required to start up a new B&B reactor with a given composition. The fuel cycle findings in [Petroski, 2011b] are used to determine achievable doubling time.

The models are created using a version of MCNPX-CINDER90 modified by TerraPower LLC to improve performance and parallelization and to add additional features specific to B&B reactors [Ellis et al., 2010], using ENDF-B/VII cross section libraries.

2.1 Quantifying material irradiation damage

One quantity that is referenced frequently in this work is Displacements Per Atom (DPA), which is a measure of radiation induced neutron damage in a material. To calculate the DPA experienced by a material, one multiplies the energy-dependent fluence experienced by a material with the appropriate DPA cross sections (which also are a function of energy), then integrates over all neutron energies. In MCNPXT, this integral is approximated by summing over the 63 energy groups present in the CINDER90 depletion code. DPA cross sections are obtained from IRDF-2002 [IAEA, 2010] for elements in steel, and from a PNNL paper [Heinisch, 2004] for silicon carbide. For steel, DPA cross sections were synthesized as a combination of 12% chromium, 0.5% nickel, and 87.5% iron, corresponding roughly to the composition of HT9 stainless steel. The DPA cross sections of different steel compositions are very similar and only differ by a few percent, so the HT9 DPA cross sections are used for all steels considered.

Measuring DPA is important because DPA directly correlates to radiation material damage effects, such as irradiation induced swelling and creep. Other measures such as fast fluence do not accurately capture the energy dependence of neutron damage. Furthermore, DPA is a constraining factor in B&B reactor design: the current DPA knowledge limit for steels is approximately 200 DPA, while a minimum-burnup B&B reactor using natural or depleted uranium as feed is likely to require over 250 DPA or more to operate. Therefore it is important to identify which B&B reactor options are able to minimize DPA and determine what fuel cycle performance is possible as a function of achievable DPA.

2.2 Assumptions and methodology for determining minimum burnup and DPA

The infinite-medium depletion approximation [Petroski, 2010a] depletes a composition according to its self generated spectrum. This spectrum is non-physical for subcritical compositions, but nevertheless it is found that the minimum burnup and DPA predicted using the method agrees well with shuffled infinite slab models of B&B reactors, which produce more realistic spectra.

To confirm the results of the infinite-medium depletion approximation, several compositions examined in this paper are modeled using a convergently-shuffled 5 cm infinite slab model. The discharge burnups of the infinite slab model in MWd/kg were set to be equal to minimum values predicted by the infinite-medium depletion calculations. Results of the comparison for a set of different U2Zr-HT9-Na compositions with different ratios of fuel, structure, and coolant are given in Table 1. The DPA and fast fluence values match very well, showing that a similar discharge fluence spectrum is experienced in the infinite-medium and infinite slab models. Meanwhile, the infinite slab models have MOEC k -effectives that are at most 1.2% higher than the predicted k_{eq} , showing that the infinite-medium depletion approximation slightly underestimates k -effective for a convergently shuffled reactor. Reactors with non-convergent shuffling schemes would have average k -effectives closer to that of the infinite-medium depletion estimate.

To further verify the results from the infinite-medium depletion approximation, a comparison of the average equilibrium cycle k -effective prediction to the MOEC k -effective from the infinite

slab model is shown in Figure 1, for two core compositions with significantly different spectra and neutronic performance: UC–SiC–He and U2Zr–HT9–PbBi. In all cases, the predicted k -effective is found to be within 1% of the modeled value.

Table 1 and Figure 1 show that the infinite-medium depletion approximation agrees well with results from an infinite slab model for a wide range of core compositions, which in turn means it is suitable for evaluating the performance of more realistic B&B systems.

Table 1. Comparison of 0D and 5 cm infinite slab model results (0D/1D infinite slab)

Composition volume fractions	100% U2Zr, 0%HT9, 0%Na	33.3% U2Zr, 0%HT9, 66.7%Na	50% U2Zr, 50%HT9, 0%Na	25% U2Zr, 25%HT9, 50%Na
Discharge burnup (MWd/kg)	66.4/ 66.4	101.8/ 101.8	140.5/ 140.5	239.1/ 239.1
Discharge burnup (FIMA*)	6.8%/ 6.8%	10.4%/ 10.4%	14.3%/ 14.3%	24.4%/ 24.3%
Discharge DPA (for HT9)	194/ 193	219/ 217	289/ 286	388/ 383
Discharge fast fluence (/cm ² s)	4.82E23/ 4.76E23	5.09E23/ 5.02E23	7.33E23/ 7.25E23	9.34E23/ 9.22E23
k_{eq} /MOEC k -effective	1.010/ 1.022	1.010/ 1.019	1.010/ 1.020	1.010/ 1.015

*Fraction of initial heavy metal atoms

Figure 1. Comparison of infinite-medium depletion approximation with infinite slab model

2.3 Assumptions and methodology for comparing core composition doubling times

The general procedure for comparing core composition doubling times is to compute the doubling time for a convergently-shuffled infinite slab model, then incorporate a capacity factor, fuel cooling time, and assumed radial power peaking factor according to the procedure shown in [Petroski, 2011b] to translate these doubling times into approximate doubling times for realistic systems.

The asymptotic reactor doubling time of a B&B reactor fleet operating in a limited-separations fuel cycle (as discussed in [Petroski, 2011b]) depends on many different factors, including core

composition, power density, capacity factor, reactor size, and several fuel cycle parameters. Core composition is important because compositions with harder neutron spectra and fewer parasitic absorbers will allow shorter doubling times. This is because better core compositions allow reactors to have lower neutron excess requirements and allow reused feed fuel to supply greater amounts of neutron excess. To evaluate the differences between core compositions, it is important to treat the other factors consistently. The different neutronic, thermal hydraulic, and fuel cycle assumptions used are given in Subsections 2.3.1 through 2.3.3.

2.3.1 Reactor neutronic models and assumptions

For each core composition studied, two models are used: an infinite-medium depletion model and a convergently-shuffled infinite slab model. First, the infinite-medium depletion model is used to determine the feed fuel discharge burnup that corresponds to an average equilibrium cycle k -effective of 1.02. Then, the infinite slab model is run using this value of discharge burnup, using a convergent shuffling pattern until an equilibrium cycle is reached. This provides values for the total neutron excess requirement as well as the total burnup contained in the equilibrium cycle. To obtain the spawning and transition timescales (defined in [Petroski, 2011b]) for this system, the only other information needed is how much starter fuel is required to initiate the equilibrium cycle. The spawning time is the amount of time needed for an equilibrium cycle B&B reactor to produce the amount of starter fuel needed to start a new B&B

reactor, while the transition time is how long it takes for a newly started B&B reactor to begin discharging used fuel at the equilibrium cycle rate.

To calculate the amount of starter fuel needed, one needs to know how much neutron excess can be obtained from reusing discharged feed fuel. One approach for doing this is to take the equilibrium cycle discharge composition from the infinite slab model and perform another infinite-medium depletion calculation [Petroski, 2011a]. This method is flexible because it allows one to change the fuel composition (e.g. remove volatile fission products) before burning it further; however it requires an additional neutronics calculation to implement. A faster but less flexible approach is to simply use the results from the earlier infinite-medium depletion run, starting from the originally calculated discharge burnup. This approach doesn't require an additional calculation, but it also assumes that the fuel composition does not change between when fuel is discharged and when it is reused.

Figure 2 compares the two approaches for an example core composition consisting of uranium, iron, and sodium. The figure shows that their results match to within a few percent, which is expected since the composition from the infinite-medium depletion is very close to the composition of the fuel discharged from the equilibrium cycle. For most of the cases studied, the infinite-medium depletion composition is used because it yields equivalent results while reducing the number of computations required; however the equilibrium cycle discharge composition is used in cases investigating the effects of cooling time and processing.

Figure 2. Neutron excess comparison between discharge feed compositions from infinite-medium and infinite slab models (infinite-medium depletion approximation)

2.3.2 Reactor thermal hydraulic models and assumptions

Based on the results of the infinite-medium depletion model and the infinite slab equilibrium cycle model, it is possible to determine the amount of starter fuel required to start up the equilibrium cycle, as well as the amount of feed and starter burnup that go into establishing the equilibrium cycle. To convert these values into fuel cycle timescales however, it is necessary to know how much power can be extracted from a given core composition: i.e. its thermal hydraulic performance. The thermal hydraulic performance of a core composition is important because a

higher power density shortens the characteristic reactor timescales that doubling time is based on.

Core thermal hydraulics is a highly complex subject in its own right; the amount of power that can be safely removed from a core depends on the fuel design and constraints on fuel and clad temperatures, coolant velocity, pressure drop across the core, and transient performance. Detailed thermal hydraulic calculations are beyond the scope of this work; instead, achievable power densities are estimated by assuming a simple model for a fuel pin, then computing maximum power based on two constraints: a coolant velocity constraint and a peak clad temperature constraint. Details about this thermal hydraulic model are given in [Petroski, 2010c].

For sodium, LBE, and helium (at 20 MPa), coolant velocity limits are assumed to be 8 m/s, 2 m/s, and 100 m/s respectively. For sodium, 8 m/s corresponds to a representative value from the IAEA database [IAEA, 2010b]. For lead/LBE coolant, 2 m/s is the limit for preventing excessive corrosion in steel [Samsonov, 1973]. For helium coolant, 100 m/s is an approximate value chosen to limit pressure drop across the core, and is comparable to the values used in the MIT helium-cooled B&B core design [Yarsky, 2005]. The coolant inlet temperature in each case is assumed to be 400°C, and the peak clad temperature is assumed to be 580°C. These assumptions are only intended to yield approximate realistic power density values for use in scoping calculations, and can be substituted with detailed thermal hydraulic calculations for actual fuel designs.

The output of the thermal hydraulic calculations is a peak *areal* power density, where areal power density is defined as the volumetric power density integrated axially at a particular radial position. Calculating areal power density is important because the correlation linking the doubling times of different shuffling sequences [Petroski, 2011b] is based on the radial power peaking factor, which is the ratio of peak to average areal power density.

Figure 3 shows how the calculated power density limits for sodium-cooled core compositions correspond to the commercial-sized reactor designs from the IAEA database. The squares in the figure correspond to the estimated peak areal power density in the different IAEA database core designs. This is calculated by dividing the total power by the core area, then multiplying by the volumetric power density peaking factor raised to the two-thirds power. Meanwhile, the triangles are the power density results calculated for the different core compositions in this study. These values depend primarily on the coolant volume fraction, and vary slightly due to the different axial power distributions in the infinite slab models (for example, a higher burnup composition would have a more spread-out power distribution and lower axial peaking, so areal power density would be slightly higher). The calculated results agree fairly well with the values for the higher power density designs, confirming that the calculated numbers are realistic.

For both sets of data, there is a clear correlation between higher coolant volume fraction and higher areal power densities, with an approximately proportional relationship between the two

for the calculated values. The reason for this proportionality can be seen from the equation linking coolant volume fraction and areal power density, shown in Equation 1:

$$Q'' = \rho c_p v \Delta T_{coolant} f_{coolant} \quad 1$$

Q'' : Areal power density, equal to the volumetric power density integrated axially
(W/m²)

ρ : Coolant density, ~844 kg/m³ at 450°C

c_p : Specific heat capacity, ~1268 J/kgK at 450°C

v : Coolant velocity, maximum assumed to be 8 m/s

$\Delta T_{coolant}$: Coolant temperature rise, less than 180K

$f_{coolant}$: Coolant volume fraction, varies depending on the core composition

In a sodium-cooled reactor, the high thermal conductivity of the sodium and clad means that the peak coolant outlet temperature is close to the peak cladding temperature of 580°C. In the cases modeled, coolant outlet temperature ranges from 555°C to 580°C, meaning that $\Delta T_{coolant}$ only varies from 155°C to 180°C. Since coolant density, specific heat capacity, velocity, and temperature rise are all approximately constant, peak areal power density is therefore

approximately proportional to the coolant volume fraction, which explains the result shown in Figure 3.

Figure 3. Areal power density vs. coolant volume fraction for sodium-cooled reactor designs

Similar figures for areal power density are shown for the LBE- and gas-cooled core compositions in their respective sections. The thermal hydraulic assumptions used in this work are not intended to substitute more rigorous analyses of actual fuel designs. Instead, they are intended as a simple means to compare homogenized core compositions that have no specific fuel design associated with them. For a more detailed design of a B&B reactor, one would want to perform detailed thermal hydraulic calculations for the core compositions being studied, and substitute

the resulting maximum areal power density results in place of the simple model results used here. Nevertheless, the simple power density calculations presented in this subsection are useful for providing realistic approximate values for reactor fleet doubling times.

2.3.3 Fuel cycle models and assumptions

In addition to reactor characteristics, fuel cycle parameters also play a role in determining doubling time. As shown in [Petroski, 2011b], how long used feed fuel spends cooling or in processing before it can be reused as starter fuel factors into the doubling time. Also, any fuel processing (such as melt refining or AIROX) that changes the fuel chemistry (e.g. by removal of volatile fission products) will change how much neutron excess can be extracted from the resulting starter fuel, which would also affect doubling time. This paper will consider both directly reused fuel (i.e. no processing other than inspection and requalification) as well as fuel which undergoes melt refining to remove a fraction of fission products.

With an areal power density limit established, one can use the procedure given in [Petroski, 2011b] to calculate the transition time t_d , and spawning time t_s for the infinite slab reactor. The studies in this chapter assume that a simple B&B reactor fuel cycle is used: i.e. each new reactor is started using a single loading of starter fuel that provides the entire neutron excess requirement for the equilibrium cycle. For this baseline fuel cycle, one can convert the spawning and transition times into a corresponding e-folding time t_e , assuming zero cooling/processing time

and a 100% capacity factor. Multiplying t_e by the natural logarithm of 2 (~ 0.693) gives the doubling time for the infinite slab reactor.

This basic result from the models is referred to as the “infinite-reactor doubling time,” since it is the doubling time that could be achieved using a hypothetical infinite reactor with a uniform power distribution (i.e. a radial power peaking factor of unity). Infinite-reactor doubling times are given in units of EFPY, since a 100% capacity factor is assumed.

To convert the infinite-reactor doubling times to more realistic doubling times, one needs to account for power peaking in finite reactors, as well as reactor capacity factor and cooling and processing time. As shown in [Petroski, 2011b], for a finite core with a reasonably flat power distribution, one can estimate its doubling time by multiplying the infinite-reactor doubling time by the radial power peaking factor of the core. This is equivalent to multiplying the two timescales t_d and t_s by the radial peaking factor. These two timescales can then be divided by the reactor capacity factor to yield timescales in years instead of EFPY. Finally, the two adjusted timescales and the cooling and processing time are used to obtain the realistic reactor doubling time. For converting infinite-reactor doubling times into realistic doubling times, this paper will assume a radial power peaking factor of 2.2 (reasonable for a large B&B reactor), a capacity factor of 90%, and a cooling and processing time of 2 years.

3. RESULTS AND DISCUSSION

The infinite-medium depletion approximation, introduced in [Petroski, 2010a] is an extremely fast and simple method that provides a fairly good estimate of the reactivity-burnup relationship for a given feed material. Because of these qualities, infinite-medium depletion calculations were used to perform a scoping study of the different reactor fuel, structure, and coolant options available. First, a detailed set of calculations is performed on a possible near term core composition consisting of U-2Zr fuel, sodium coolant, and HT-9 structure, to determine the effect that coolant and structure volume fractions have on minimum burnup results. Finally, an overview of a variety of fuel, structure, and coolant combinations is considered to narrow down the number of possible candidate core compositions for the doubling time studies in sections 3.2 through 3.4.

Different fuel options were investigated in [Petroski, 2010a], where it was determined that the most interesting fuels for further study are U2Zr, U10Zr, U₃Si₂, U¹⁵N, and UC. As a result, these fuel types are the options considered in this study.

3.1 Determining minimum burnup requirements

By using the infinite-medium depletion approximation, it is possible to quickly investigate the minimum burnup and fluence for a range of core compositions. This is useful for creating a map of what core compositions satisfy desired limits on burnup or DPA. As an example of doing

this, an example study was performed for a core composition consisting of U2Zr fuel, HT9 structure, and sodium coolant. HT9 structure and sodium coolant were considered because they represent potential near-term options for a B&B reactor, since there is already experience using them in fast reactor applications. Meanwhile, there is little irradiation data for low zirconium alloys fuels such as U2Zr, so fuel testing would be required to qualify it for use in a reactor.

Second, the infinite-medium depletion approximation was applied to a wide range of core compositions involving different fuel, structure, and coolant options, in order to obtain a narrowed down list of promising options.

3.1.1 Investigating a range of U2Zr–Na–HT9 core compositions

The composition for HT9 is taken from a paper by Klueh [Klueh, 2005]. Its density at room temperature (7.7924 g/cc) is from a paper by Gelles [Gelles, 94]. Sodium density is assumed to be 0.844 g/cc, corresponding to a coolant temperature of 450°C, a representative average reactor temperature. Room temperature densities are used for fuel and structure materials because their thermal expansion coefficients are small compared to the coolant. In all cases the ENDF-B/VII cross section libraries at 900K are used.

Runs were performed for sodium volumes from 0 to 2 times the fuel volume in intervals of 0.2, and for HT9 volumes from 0 to 1 times in the fuel volume of intervals of 0.1. The target average

equilibrium cycle k_{eq} is 1.01 in all cases (k_{eq} is defined in [Petroski, 2011a], and corresponds roughly with the uncontrolled k -effective for a B&B reactor with very low leakage). To run the 121 cases, a Python script was written that allows the creation of a large number of infinite-medium MCNPXT input decks from a spreadsheet, allowing a large number of different compositions to be rapidly investigated. Another script was written that allows minimum burnup, DPA, and fast fluence data to be collected from the output files.

Contour plots for minimum required burnup and DPA for the range of core compositions are given in Figures 4 and 5. The slope of the contours is different in the two plots, reflecting the differences in spectra among the runs. The addition of sodium creates a softer spectrum, so for two compositions with the same total DPA, the composition with more sodium will have higher burnup, due to higher total fluence. Even without sodium or HT9 present, DPA levels are already close to or past current knowledge limits (~200 DPA). However, if further testing is able to provide a 50% or 100% increase in these experimental limits, the range of possible core compositions expands enormously.

Figure 4. Minimum burnup for different U₂Zr-Na-HT9 core compositions (FIMA)

Figure 5. Minimum DPA for different U₂Zr-Na-HT9 core compositions (HT9 DPA cross sections)

3.1.2 Investigating a wide variety of core compositions

Fuels considered are the neutronicallly promising fuels from the earlier fuels results (U2Zr, U10Zr, U₃Si₂, UN enriched in ¹⁵N, and UC). Candidate structural materials for fast reactors are the ferritic/martensitic steels HT9 and T91, oxide dispersion-strengthened steel (ODS) (e.g. MA956, assumed in this study) and silicon carbide. Candidate fast reactor coolants are sodium, lead, lead-bismuth eutectic (LBE), helium, and CO₂.

The assumed densities of the fuel compositions are given in [Petroski, 2010a]. Compositions and densities for the structural materials are given in Table 2. Silicon carbide is modeled using its stoichiometric composition and its monolithic density; a silicon-carbide composite may have a slightly lower density and include trace elements from its manufacture. The densities for the structural materials are all at room temperature; thermal expansion would have a small effect on the relative number densities of fuel, structure, and coolant. The sodium, lead, and LBE densities (0.844, 10.52, and 10.13 g/cc respectively) are at a representative core operating temperature of 450°C, while the helium and CO₂ densities (0.0125 and 0.133 g/cc) assume core average temperatures and pressures of 500°C and 20 MPA. The LBE composition is assumed to be at the eutectic point (44.5% Pb, 55.5% Bi).

Table 2. Compositions and densities for structural materials

Material	HT9	T91	MA956 (ODS)	SiC
Density g/cc	7.79	7.74	7.25	3.21
Element weight %				
Fe	84.55	88.77	73.665	
Cr	12	9	20	
C	0.2	0.1	0.05	30.0
Mo	1	1		
Si	0.4	0.4		70.0
W	0.5			
V	0.25	0.2		
Mn	0.6	0.4	0.15	
Al			4.75	
Ti			0.4	
Y			0.39	
O			0.11	
Ni	0.5		0.25	
P			0.01	
Nb		0.08		
Cu			0.075	
Co			0.15	
N		0.05		

For simplicity, the respective volume fractions of the fuel, structure, and coolant are kept constant, at 1 part fuel, 0.6 parts structure, and 1 part coolant. These fractions are roughly equal to the average values given for fast reactors in the IAEA fast reactor database. For B&B reactors, it would likely be desirable to use smaller amounts of structure and coolant to minimize the total burnup and fluence experienced by the fuel. The IAEA fast reactor database does not give information on the amount of bond material used with metal fuels. Some bond material (a liquid metal such as sodium or lead) is typically used with metal fuels to decrease thermal resistance between the fuel and clad. Bond is not modeled in this study; its effect would be

similar to the same amount of coolant material, although bond may become squeezed out fairly early in the fuel's depletion as the fuel swells.

With 5 fuel types, 4 structure types, and 5 coolant types, a total of 100 different core compositions were studied using the infinite-medium depletion model. Results for the minimum burnup (target average k_{eq} of 1.01), minimum DPA (evaluated using HT9 DPA cross sections in all cases, and SiC DPA cross sections in the cases with SiC), and minimum fast fluence are given in Tables 3 through 7, separated by coolant type.

Because a significant number of compositions did not reach a predicted k_{eq} of 1.01, Tables 3 through 7 also give results for the maximum k_{eq} reached by a composition. The maximum k_{eq} corresponds to the point on a neutron excess curve which maximizes the slope of a line connecting the curve with the origin. Figures 6 and 7 give schematic illustrations of where the maximum k_{eq} occurs on a neutron excess curve, for cases with the maximum k_{eq} above and below unity. The maximum k_{eq} occurs at a point before the maximum neutron excess when the maximum k_{eq} is greater than unity. The maximum k_{eq} is a good dimensionless figure of merit for the neutronic performance of a fuel composition, and is included in the below tables to give better differentiation of the various cases. It is a better figure of merit than maximum neutron excess (ΔN), since maximum ΔN is a dimensional quantity that depends on factors like the total heavy metal density.

Figure 6. Schematic illustration of maximum k_{eq} (>1) on neutron excess curve

Figure 7. Schematic illustration of maximum k_{eq} (<1) on neutron excess curve

Table 3. Minimum burnup for helium-cooled core compositions (for $k_{eq} = 1.01$)

Fuel	Structure	Coolant	Minimum burnup fraction	Minimum DPA (HT9/SiC DPA)	Minimum fast fluence (/cm ² s)(>0.1 MeV)	Maximum k_{eq}
U2Zr	ODS	He	11.2%	245	6.11E+23	1.120
U2Zr	T91	He	11.2%	249	6.28E+23	1.119
U2Zr	HT9	He	11.4%	250	6.30E+23	1.118
U2Zr	SiC	He	12.8%	271	5.09E+23	1.103
U10Z	ODS	He	14.4%	284	7.07E+23	1.080
U10Z	T91	He	14.5%	290	7.30E+23	1.078
U10Z	HT9	He	14.7%	293	7.36E+23	1.076
U10Z	SiC	He	17.2%	322	6.03E+23	1.060
U3Si	ODS	He	17.3%	302	7.29E+23	1.057
U3Si	T91	He	17.4%	309	7.58E+23	1.055
U3Si	HT9	He	17.8%	314	7.67E+23	1.053
U3Si	SiC	He	20.7%	333	6.31E+23	1.037
UN15	ODS	He	20.8%	326	7.36E+23	1.037
UN15	T91	He	21.3%	336	7.63E+23	1.034
UN15	HT9	He	21.7%	341	7.75E+23	1.032
UN15	SiC	He	23.4%	355	6.59E+23	1.026
UC	ODS	He	26.5%	389	8.61E+23	1.017
UC	T91	He	27.6%	407	9.08E+23	1.015
UC	HT9	He	28.9%	426	9.48E+23	1.013
UC	SiC	He	N/A	N/A	N/A	1.010

Table 4. Minimum burnup for CO₂-cooled core compositions (for $k_{eq} = 1.01$)

Fuel	Structure	Coolant	Minimum burnup fraction	Minimum DPA (HT9/SiC DPA)	Minimum fast fluence (/cm ² s)(>0.1 MeV)	Maximum k_{eq}
U2Zr	ODS	CO2	11.6%	248	6.07E+23	1.114
U2Zr	T91	CO2	11.6%	251	6.22E+23	1.114
U2Zr	HT9	CO2	11.8%	253	6.25E+23	1.112
U2Zr	SiC	CO2	13.1%	273	5.10E+23	1.099
U10Z	ODS	CO2	15.1%	290	7.09E+23	1.075
U10Z	T91	CO2	15.2%	296	7.30E+23	1.073
U10Z	HT9	CO2	15.5%	299	7.37E+23	1.071
U10Z	SiC	CO2	17.8%	327	6.10E+23	1.055
U3Si	ODS	CO2	18.3%	312	7.34E+23	1.050
U3Si	T91	CO2	18.6%	320	7.65E+23	1.048
U3Si	HT9	CO2	18.9%	324	7.75E+23	1.046
U3Si	SiC	CO2	21.7%	340	6.44E+23	1.033
UN15	ODS	CO2	21.8%	337	7.51E+23	1.032
UN15	T91	CO2	22.4%	347	7.81E+23	1.029
UN15	HT9	CO2	22.8%	353	7.93E+23	1.028
UN15	SiC	CO2	24.2%	365	6.72E+23	1.023
UC	ODS	CO2	28.7%	417	9.12E+23	1.013
UC	T91	CO2	31.6%	460	1.01E+24	1.011
UC	HT9	CO2	N/A	N/A	N/A	1.009
UC	SiC	CO2	N/A	N/A	N/A	1.007

Table 5. Minimum burnup for LBE-cooled core compositions (for $k_{eq} = 1.01$)

Fuel	Structure	Coolant	Minimum burnup fraction	Minimum DPA (HT9/SiC DPA)	Minimum fast fluence (/cm ² s)(>0.1 MeV)	Maximum k_{eq}
U2Zr	ODS	PbBi	13.0%	271	6.83E+23	1.097
U2Zr	T91	PbBi	13.1%	275	7.01E+23	1.095
U2Zr	HT9	PbBi	13.2%	276	7.02E+23	1.094
U2Zr	SiC	PbBi	15.1%	311	5.87E+23	1.079
U10Z	ODS	PbBi	17.4%	325	8.17E+23	1.056
U10Z	T91	PbBi	17.6%	333	8.46E+23	1.053
U10Z	HT9	PbBi	17.9%	336	8.52E+23	1.051
U10Z	SiC	PbBi	21.6%	390	7.37E+23	1.035
U3Si	ODS	PbBi	22.4%	368	8.97E+23	1.029
U3Si	T91	PbBi	22.9%	379	9.38E+23	1.027
U3Si	HT9	PbBi	23.5%	387	9.56E+23	1.025
U3Si	SiC	PbBi	33.6%	470	9.89E+23	1.010
UN15	ODS	PbBi	29.4%	435	1.00E+24	1.013
UN15	T91	PbBi	31.8%	473	1.10E+24	1.010
UN15	HT9	PbBi	N/A	N/A	N/A	1.009
UN15	SiC	PbBi	N/A	N/A	N/A	1.002
UC	ODS	PbBi	N/A	N/A	N/A	0.994
UC	T91	PbBi	N/A	N/A	N/A	0.992
UC	HT9	PbBi	N/A	N/A	N/A	0.990
UC	SiC	PbBi	N/A	N/A	N/A	0.986

Table 6. Minimum burnup for lead-cooled core compositions (for $k_{eq} = 1.01$)

Fuel	Structure	Coolant	Minimum burnup fraction	Minimum DPA (HT9/SiC DPA)	Minimum fast fluence (/cm ² s)(>0.1 MeV)	Maximum k_{eq}
U2Zr	ODS	Pb	13.2%	273	6.86E+23	1.095
U2Zr	T91	Pb	13.3%	277	7.04E+23	1.093
U2Zr	HT9	Pb	13.4%	279	7.09E+23	1.091
U2Zr	SiC	Pb	15.3%	314	5.90E+23	1.077
U10Z	ODS	Pb	17.8%	330	8.28E+23	1.053
U10Z	T91	Pb	17.9%	336	8.54E+23	1.051
U10Z	HT9	Pb	18.2%	340	8.60E+23	1.049
U10Z	SiC	Pb	22.0%	397	7.46E+23	1.033
U3Si	ODS	Pb	23.2%	378	9.22E+23	1.026
U3Si	T91	Pb	23.8%	391	9.68E+23	1.024
U3Si	HT9	Pb	24.3%	398	9.81E+23	1.022
U3Si	SiC	Pb	N/A	N/A	N/A	1.008
UN15	ODS	Pb	31.3%	460	1.06E+24	1.011
UN15	T91	Pb	N/A	N/A	N/A	1.008
UN15	HT9	Pb	N/A	N/A	N/A	1.007
UN15	SiC	Pb	N/A	N/A	N/A	1.000
UC	ODS	Pb	N/A	N/A	N/A	0.992
UC	T91	Pb	N/A	N/A	N/A	0.990
UC	HT9	Pb	N/A	N/A	N/A	0.988
UC	SiC	Pb	N/A	N/A	N/A	0.984

Table 7. Minimum burnup for sodium-cooled core compositions (for $k_{eq} = 1.01$)

Fuel	Structure	Coolant	Minimum burnup fraction	Minimum DPA (HT9/SiC DPA)	Minimum fast fluence (/cm²s)(>0.1 MeV)	Maximum k_{eq}
U2Zr	ODS	Na	13.4%	262	6.36E+23	1.092
U2Zr	T91	Na	13.5%	266	6.51E+23	1.091
U2Zr	HT9	Na	13.6%	267	6.53E+23	1.089
U2Zr	SiC	Na	15.1%	291	5.48E+23	1.077
U10Z	ODS	Na	18.5%	321	7.75E+23	1.048
U10Z	T91	Na	18.8%	329	8.03E+23	1.046
U10Z	HT9	Na	19.0%	332	8.09E+23	1.045
U10Z	SiC	Na	22.1%	373	7.05E+23	1.031
U3Si	ODS	Na	25.3%	387	9.02E+23	1.020
U3Si	T91	Na	26.0%	401	9.48E+23	1.017
U3Si	HT9	Na	27.0%	415	9.78E+23	1.015
U3Si	SiC	Na	N/A	N/A	N/A	1.005
UN15	ODS	Na	N/A	N/A	N/A	1.006
UN15	T91	Na	N/A	N/A	N/A	1.004
UN15	HT9	Na	N/A	N/A	N/A	1.002
UN15	SiC	Na	N/A	N/A	N/A	1.000
UC	ODS	Na	N/A	N/A	N/A	0.988
UC	T91	Na	N/A	N/A	N/A	0.986
UC	HT9	Na	N/A	N/A	N/A	0.984
UC	SiC	Na	N/A	N/A	N/A	0.984

Comparing the results in Tables 3 through 7, several conclusions can be drawn. Gas coolants are clearly the best neutronically, with helium being slightly superior to CO₂. The metal coolants all perform similarly, with LBE being slightly better than pure lead. Sodium requires higher burnup than LBE or lead, but creates a softer spectrum that reduces the minimum required DPA and fast fluence. The three different steel structural materials each exhibit very similar performance, with ODS being slightly better neutronically (in terms of minimum burnup and maximum k_{eq}) per unit volume than T91, which is slightly better than HT9. Choice of which to use would likely depend primarily on the materials' irradiation performance at high temperature. Silicon carbide requires a somewhat higher burnup than the steels, but has a moderating effect that reduces the required

fast fluence. This allows the core compositions using SiC to experience lower fast fluence, as seen in Figure 8.

The largest factor in determining neutronic performance is the fuel type. Ceramic fuels perform significantly worse than metal ones, as the softer spectra they cause create additional parasitic absorptions and lower the equilibrium ratio of Pu-239 to U-238. Despite its lower heavy metal density, the intermetallic U_3Si_2 is the best performing compound fuel, although its melting point ($1650^{\circ}C$) is lower than that of UC ($2400^{\circ}C$) and UN ($2650^{\circ}C$), losing some of the benefit of using ceramic fuel. The poor neutronic performance of compound fuels in B&B systems means that they would only be used in cases where their other properties were important, such as their refractory properties in high temperature applications. The neutronic gain achieved by switching from a liquid metal to a gas coolant is lost if such a switch requires use of a ceramic fuel instead of a metal fuel. Metal fuels show much better neutronic performance, and notably the amount of alloying material also makes a significant difference. The neutronic advantage of metal fuels shown here would be offset somewhat by the fact that metal fuels require a lower smear density (to accommodate fuel swelling) and bond material (to reduce fuel temperatures).

Based on these results, a subset of core composition options was selected for the more in depth doubling time study. The coolant options chosen were sodium, lead-bismuth eutectic, and helium. Each of these coolant options yields significantly different thermal hydraulic performance, while LBE and lead have similar neutronic performance, as do helium and CO_2 . For structure, T91 and silicon carbide were chosen. All the steel structural materials have similar

neutronic performance, and T91 is a likely near-future choice while having neutronic performance between those of the other two types of steel. Silicon carbide has significantly different neutronic performance from the steels because it causes neutron spectrum softening. For fuels, U₂Zr, U₁₀Zr, and U₃Si₂ were chosen. U₂Zr and U₁₀Zr were selected to examine the effect of alloy composition in a metal fuel, while U₃Si₂ was selected because it is the best performing compound fuel.

Figure 8. Minimum fast fluence vs. minimum burnup for viable core compositions

3.2 Determining reactor fleet doubling times

From the selection of different possible fuel, structure, and coolant materials, a combination of infinite-medium and infinite slab models are used to form an estimate of achievable doubling time for each. For each composition, different amounts of coolant and structure are considered,

and the effect of fuel recycling via melt refining is also investigated. Results are divided among the different coolants, since the different thermal hydraulic performance of each has a major effect on doubling times.

3.2.1 Sodium fast reactor core compositions

A total of 96 different sodium fast reactor core compositions are evaluated. Three types of fuel were considered: U2Zr, U10Zr, and U3Si2, representing low and high percentage metal alloy fuel and the best performing compound fuel. The metal fuel is assumed to have a smear density of 75% while the compound fuel is assumed to have a smear density of 90%, keeping in mind the relatively high burnup levels required for B&B operation. Two types of structural materials are considered: T91 and SiC.

Compositions in this and the following sections are named using the following convention: fuel type – fuel volume – structure type – structure volume – coolant type – coolant volume. For example, the core composition U2Zr-75-T91-30-Na-100 is a core composition consisting of 75 parts U2Zr fuel, 30 parts T91 structure, and 100 parts sodium coolant by volume. The different volume numbers are normalized so that 100 corresponds to the volume available inside the cladding, or “available fuel volume,” so the fuel volume number is equal to the fuel smear density in percent. This means that the above composition also contains 25 parts void by volume, which reduces the overall density of the composition.

Two different amounts of structural material are examined: 30 volume percent of the available fuel volume and 60 volume percent of the available fuel volume. The low amount of structure is similar to that of an inverted-fuel (i.e. coolant channels going through blocks of fuel) gas-cooled fast reactor design [Pope, 2006] which has a structure to fuel volume ratio of 22%. If one assumes that roughly 1 cm of structure is required every 20 cm to form the top and bottom of each fuel subassembly (i.e. 5% of the fuel is replaced by structure), then this ratio increases to 29%, which is rounded up to 30%. The 60% case assumes that much more structure is needed to form the clad, duct, and subassembly coupling structures, or to accommodate a different fuel form (e.g. pins instead of inverted fuel). Here the core is made up of axially-separated “fuel subassemblies” because this analysis pertains to “minimum-burnup” B&B reactors in which fuel can be shuffled in all three dimensions.

For each of the twelve fuel–structure combinations, eight different amounts of sodium coolant are considered, from 50% of the available fuel volume up to 225%, in 25% increments. Bond material between clad and fuel (which may be used with metal fuel) is not explicitly modeled, although some of the sodium in the model can be attributed to bond.

Of the 96 different compositions considered, 52 are capable of reaching the target infinite-medium k_{eq} prediction of 1.02; these compositions are then studied using infinite slab models operating in a B&B equilibrium cycle. Equilibrium cycle results for the different compositions are given in Tables 8 and 9. The two tables give results for compositions with T91 and silicon carbide structure respectively. Material radiation damage is represented as the DPA that a sample of HT9 or silicon carbide would experience. Axial length is the length over which

99.75% of total neutron absorptions occur, using an exponential interpolation between the 5 cm zones. “MOEC BU contained” is the amount of burnup present at the middle of the equilibrium cycle, which is important for determining how many full power years are required to establish the equilibrium cycle. The adjusted ΔN is the neutron excess requirement of the equilibrium cycle, which is proportional to the amount of starter fuel needed. Each bordered section contains a particular fuel and structure combination with different amounts of coolant.

Table 8. Equilibrium cycle results for sodium-cooled core compositions (T91 structure)

Composition	HM density (g/cc)	Ave. k_{eq}	Discharge burnup fraction	Discharge HT9 DPA	Discharge fast fluence (/cm ²) (>0.1 MeV)	Axial length (cm)	MOEC BU contained (MWy/m ²)	Adjusted ΔN (mol/m ²)
U2Zr-75-T91-30-Na-50	7.47	1.029	11.6%	247	6.07E+23	205	947	1197
U2Zr-75-T91-30-Na-75	6.56	1.028	12.4%	253	6.14E+23	226	980	1209
U2Zr-75-T91-30-Na-100	5.85	1.028	13.3%	261	6.27E+23	246	1034	1233
U2Zr-75-T91-30-Na-125	5.27	1.027	14.2%	268	6.38E+23	261	1082	1249
U2Zr-75-T91-30-Na-150	4.80	1.028	15.4%	278	6.57E+23	277	1145	1276
U2Zr-75-T91-30-Na-175	4.41	1.026	16.4%	287	6.73E+23	296	1207	1287
U2Zr-75-T91-30-Na-200	4.08	1.027	17.7%	299	6.98E+23	308	1295	1318
U2Zr-75-T91-30-Na-225	3.79	1.028	19.2%	314	7.27E+23	324	1400	1345
U10Zr-75-T91-30-Na-50	6.00	1.028	15.1%	291	7.11E+23	227	1143	1287
U10Zr-75-T91-30-Na-75	5.27	1.028	16.6%	304	7.34E+23	247	1231	1314
U10Zr-75-T91-30-Na-100	4.70	1.027	18.2%	318	7.61E+23	269	1336	1350
U10Zr-75-T91-30-Na-125	4.24	1.027	20.2%	338	8.01E+23	290	1476	1389
U10Zr-75-T91-30-Na-150	3.86	1.026	22.6%	363	8.55E+23	310	1677	1426
U10Zr-75-T91-30-Na-175	3.54	1.025	26.0%	402	9.40E+23	334	1991	1466
U3Si2-90-T91-30-Na-50	5.66	1.028	15.9%	286	6.77E+23	236	1180	1301
U3Si2-90-T91-30-Na-75	4.97	1.028	17.6%	302	7.05E+23	257	1288	1335
U3Si2-90-T91-30-Na-100	4.43	1.028	19.5%	320	7.42E+23	279	1420	1373
U3Si2-90-T91-30-Na-125	3.99	1.027	21.8%	343	7.87E+23	302	1595	1406
U3Si2-90-T91-30-Na-150	3.64	1.026	25.0%	378	8.63E+23	326	1886	1456
U2Zr-75-T91-60-Na-50	6.41	1.028	15.5%	295	7.32E+23	217	1202	1337
U2Zr-75-T91-60-Na-75	5.72	1.028	16.7%	306	7.50E+23	233	1270	1358
U2Zr-75-T91-60-Na-100	5.17	1.027	18.2%	320	7.77E+23	252	1366	1385
U2Zr-75-T91-60-Na-125	4.72	1.027	19.8%	335	8.07E+23	267	1479	1409
U2Zr-75-T91-60-Na-150	4.34	1.026	21.6%	354	8.48E+23	285	1632	1441
U2Zr-75-T91-60-Na-175	4.02	1.026	24.0%	381	9.06E+23	305	1840	1478
U2Zr-75-T91-60-Na-200	3.74	1.025	27.4%	422	9.98E+23	326	2203	1514
U10Zr-75-T91-60-Na-50	5.14	1.026	23.2%	394	9.73E+23	242	1796	1502
U10Zr-75-T91-60-Na-75	4.60	1.025	27.9%	452	1.11E+24	270	2300	1560
U3Si2-90-T91-60-Na-50	4.85	1.026	26.2%	414	9.93E+23	256	2084	1538

Table 9. Equilibrium cycle results for sodium-cooled core compositions (SiC structure)

Composition	HM density (g/cc)	Ave. k_{eq}	Discharge burnup fraction	Discharge SiC DPA	Discharge fast fluence (/cm ²) (>0.1 MeV)	Axial length (cm)	MOEC BU contained (MWy/m ²)	Adjusted ΔN (mol/m ²)
U2Zr-75-SiC-30-Na-50	7.47	1.029	12.6%	279	5.23E+23	192	978	1183
U2Zr-75-SiC-30-Na-75	6.56	1.030	13.4%	288	5.39E+23	213	1023	1196
U2Zr-75-SiC-30-Na-100	5.85	1.028	14.3%	295	5.51E+23	231	1068	1212
U2Zr-75-SiC-30-Na-125	5.27	1.029	15.3%	305	5.68E+23	249	1128	1236
U2Zr-75-SiC-30-Na-150	4.80	1.027	16.3%	314	5.84E+23	267	1189	1248
U2Zr-75-SiC-30-Na-175	4.41	1.028	17.5%	327	6.08E+23	283	1267	1273
U2Zr-75-SiC-30-Na-200	4.08	1.028	18.7%	339	6.28E+23	301	1353	1296
U2Zr-75-SiC-30-Na-225	3.79	1.027	20.0%	354	6.56E+23	318	1446	1314
U10Zr-75-SiC-30-Na-50	6.00	1.028	16.7%	331	6.17E+23	213	1218	1282
U10Zr-75-SiC-30-Na-75	5.27	1.028	18.3%	346	6.44E+23	236	1319	1310
U10Zr-75-SiC-30-Na-100	4.70	1.028	20.1%	365	6.78E+23	258	1448	1347
U10Zr-75-SiC-30-Na-125	4.24	1.027	22.3%	391	7.25E+23	280	1626	1377
U10Zr-75-SiC-30-Na-150	3.86	1.026	25.3%	423	7.82E+23	302	1898	1413
U3Si2-90-SiC-30-Na-50	5.66	1.029	17.3%	315	5.86E+23	223	1261	1291
U3Si2-90-SiC-30-Na-75	4.97	1.028	18.9%	332	6.17E+23	249	1370	1320
U3Si2-90-SiC-30-Na-100	4.43	1.028	21.0%	351	6.50E+23	272	1527	1355
U3Si2-90-SiC-30-Na-125	3.99	1.027	23.4%	378	6.99E+23	297	1724	1384
U3Si2-90-SiC-30-Na-150	3.64	1.026	27.2%	419	7.74E+23	323	2099	1427
U2Zr-75-SiC-60-Na-50	6.41	1.028	18.3%	327	6.01E+23	196	1342	1305
U2Zr-75-SiC-60-Na-75	5.72	1.029	19.8%	342	6.28E+23	213	1447	1337
U2Zr-75-SiC-60-Na-100	5.17	1.027	21.4%	358	6.58E+23	233	1577	1356
U2Zr-75-SiC-60-Na-125	4.72	1.026	23.4%	380	6.98E+23	253	1742	1380
U2Zr-75-SiC-60-Na-150	4.34	1.025	25.9%	409	7.50E+23	274	1991	1412

A few trends can be observed in Tables 8 and 9. All the cases show an average k_{eq} between 1.025 and 1.029, consistently slightly higher than infinite-medium prediction of 1.020. This means that some additional core compositions may have been able to reach an average k_{eq} of 1.020, but such core compositions would be barely viable and have extremely long doubling times. The low-structure-fraction SiC cases show higher discharge burnups than the corresponding low-structure T91 cases, but due to their softer spectra have lower fast fluence. The SiC cases exhibit higher DPA because SiC has somewhat (10-20%) higher DPA cross sections than steel in the spectra considered. The shorter neutron mean free paths due to a softer spectrum also reduces the axial length of the SiC cases. Shifting to higher Zr alloy fuel or U₃Si₂ fuel greatly penalizes neutronic performance, as does going from a low structure fraction to a high structure fraction. Increasing the sodium volume fraction has a more gradual effect.

Starting from the 52 candidate core compositions in Tables 8 and 9, several situations are analyzed. First, a case is considered in which fuel is directly transferred from one reactor to another, with no processing or recladding of any kind. In this case, the structural material accumulates DPA over two reactor passes, first as feed fuel in one B&B reactor, then again as starter fuel in an additional B&B reactor. Second, a case is considered in which the fuel can be reclad before it is reloaded into a second reactor, which effectively resets the amount of DPA accumulated on the structural material. The goal in these two cases is to determine reactor doubling time as a function of peak DPA for each of the fuel-structure combinations considered, leaving the amount of coolant as a variable to optimize. Afterward, additional cases are considered to evaluate the effect of using natural vs. depleted uranium, accounting for fission gas escape during irradiation, and the use of melt refining to refabricate metal fuel.

3.2.1.1 Doubling time vs. DPA without recladding

This first portion of the sodium core composition evaluation considers cases with no fuel refabrication or processing of any kind. It is assumed that fuel and structure can be burned up to an ultimate DPA limit, after which it is disposed. Given a DPA limit, it is assumed fuel discharged from the equilibrium cycles in Tables 8 and 9 is then further burned to the ultimate DPA limit when it is reused as starter fuel. The baseline B&B limited-separations fuel cycle is assumed, in which new reactors are started as sufficient starter fuel becomes available. The doubling time can then be computed as shown in [Petroski, 2011b]. Note that in this no-

processing case, it is assumed that fuel is capable of being burned significantly beyond its equilibrium cycle minimum burnup, therefore the “reactor growth” fuel cycle option discussed in [Petroski, 2011b] could potentially be used to reduce reactor doubling times.

For each particular fuel/structure combination, the coolant volume fraction is varied and the infinite-reactor doubling time is calculated as a function of the ultimate DPA limit. Results for U2Zr fuel with a low volume fraction of T91 structure are shown in Figure 9. The figure shows that there is an optimum coolant fraction that minimizes doubling time. Too low a coolant fraction means lower power density (as shown in Figure 3), while too high a coolant fraction reduces the neutron excess produced. The curves in Figure 9 have a minimum near where the coolant volume is equal to 100% of the available fuel volume. The minimum gradually trends toward lower volumes of coolant as the DPA limit decreases, which reduces the neutron excess extractable from a given amount of fuel.

Figure 9. Infinite-reactor doubling time as a function of coolant volume for different ultimate DPA limits (U2Zr with low structure fraction T91)

A similar optimal doubling time search was performed for each of the fuel/structure combinations considered, yielding the infinite-reactor doubling time as a function of ultimate DPA limit. An overview of the results for T91 structure is shown in Figure 10. The legend gives the type of fuel and type of structure, with the numbers being the volume of the fuel/structure relative to the available fuel volume. Each of the points in the figure corresponds to a minimum from a curve like the ones given in Figure 9.

A semi-log plot is used in Figure 10 because of the large difference between the best and worst compositions. The multiple-century doubling times for some of the compositions are a result of the very low k -infinity in some of the reused compositions, which means that the composition

can only supply a small amount of neutron excess. As a result, an enormous amount of starter fuel is required to provide the needed neutron excess for a new equilibrium cycle, which greatly increases both the spawning and transition times. These immense fuel requirements would likely make fuel cycles with such compositions unachievable in practice.

Figure 10 also shows some that some of the doubling times increase with higher DPA. This is because these compositions have been burned to the point that burning the fuel further begins to cost neutrons instead of supplying them; i.e. the fuel has passed the maximum on Figure 6 and has become a neutron absorber.

Figure 10. Infinite-reactor doubling time as a function of ultimate DPA for sodium-cooled core compositions with T91 structure

Figure 11 shows an expanded view of the best performing compositions using T91, and Figure 12 gives the corresponding chart for compositions using silicon carbide. The best performing compositions are those with the least non-fuel material: the low structure volume T91 and SiC cases using low alloy percent metal fuels. These can achieve potentially <5 EFPY (Effective Full Power Year) doubling times in an infinite reactor, corresponding to a ~10 EFPY doubling time in a finite reactor with a radial power peaking factor of about 2.0. The doubling times in these cases plateau at a DPA of about 450-500; burning the core composition further decreases doubling time only slightly.

Any changes from these two best-case compositions greatly worsen doubling time. For example, switching to the higher zirconium fraction fuel U10Zr in the T91 structure case causes the doubling time to approximately double, while switching to the compound fuel U_3Si_2 increases the doubling time even further. Increasing the amount of T91 from 30% to 60% of the available fuel volume also causes doubling times to more than double. Combining two of these changes, e.g. switching to a high-zirconium-fraction metal fuel and increasing the T91 volume fraction causes doubling times to reach close to a century, as shown in Figure 10. The decrease in performance is even more severe for the SiC clad cases, as the softer spectrum in those cases increases the amount of parasitic absorptions in non-fuel materials. Doubling the amount of SiC present causes infinite-reactor doubling times to rise to over 15 years.

Figures 13 and 14 translate these results into realistic reactor doubling times, assuming a radial power peaking factor of 2.2, a capacity factor of 90%, and a fuel cooling time of one year before it can be reused. A 2.2 radial peaking factor is a reasonable value for a large power reactor, as

shown in [Petroski, 2011b]. The realistic doubling times are computed by multiplying the infinite-reactor timescales by the radial peaking factor and dividing by the capacity factor, then including the cooling time. The figure shows that B&B reactors with no fuel processing can potentially achieve fairly rapid doubling times (~10 years), but doing so requires a number of technical advancements. These include demonstrating structural materials at high (400+) DPA, developing low-zirconium-fraction metal fuel, and designing a low-structure-volume-fraction core that allows for high power density operation, high burnup, and three-dimensional fuel shuffling.

The maximum areal power densities assumed for the SiC core compositions in Figures 13 and 14 are the same as those for the T91 core compositions. One of the potential advantages of SiC is its potential to operate at higher temperatures and power densities. If SiC can in fact be irradiated to high DPA while enabling higher power densities, then it would be capable of producing shorter reactor doubling times than core compositions using T91. Similarly, if advanced steels (such as oxide-dispersion strengthened steels) are capable of high temperature, high DPA operation, then doubling times would similarly be shorter for those materials.

Figure 11. Infinite-reactor doubling time as a function of ultimate DPA for sodium-cooled core compositions with T91 structure (detail)

Figure 12. Infinite-reactor doubling time as a function of ultimate DPA for sodium-cooled core compositions with SiC structure (detail)

Figure 13. Realistic reactor doubling times as a function of ultimate DPA for sodium-cooled core compositions with T91 structure (no recladding)

Figure 14. Realistic reactor doubling times as a function of ultimate DPA for sodium-cooled core compositions with SiC structure (no recladding)

3.2.1.2 Doubling time vs. DPA with recladding

Subsection 3.2.1.2 considered sodium-cooled B&B reactors in a fuel cycle with no fuel processing of any kind: used fuel elements would be transferred intact from one generation of reactors to start up the next. With no processing, DPA levels of 400+ are required for realistic reactor doubling times on the order of 10-15 years, and only for the best fuel-structure combinations: low-alloy (2 wt% Zr) uranium-zirconium fuel with a low volume fraction (30% of available fuel volume) of T91 or SiC structure.

In this subsection, an additional analysis is performed in which fuel recladding occurs between the two reactor passes. In this analysis, a core composition is first burned to its equilibrium cycle burnup and DPA in an initial pass, and then undergoes a recladding process that allows the fuel to be irradiated further. This process could consist of just mechanical recladding or also include more complex processes like melt refining. Full chemical reprocessing is an option as well, but is not considered here because the goal of this work is to characterize limited-separations fuel cycles that do not use full reprocessing.

In terms of modeling the recladding step, it is again assumed that no change to fuel chemistry or geometry occurs during processing. With this assumption, a single infinite-medium depletion calculation is sufficient to approximate the behavior of the fuel through both reactor passes, as discussed in Subsection 2.3.1. As a result, the only effect of the recladding process is to reset the DPA accumulated by the structure. This simplified assumption doesn't take into account: a)

changes in fuel composition due to radioactive decay during the processing period; b) changes in fuel composition due to processing; and c) changes in fuel geometry during processing. The first two factors, compositional changes during cooling/processing, are considered in more detail in Subsection 3.2.1.3.

The third factor, changes in fuel geometry, can occur if the fuel smear density, structure fraction, or coolant fraction are changed during fuel processing. In this study, a “direct-transfer” simplification is used, which assumes that there is no change in geometry, and fuel is treated as if it is directly transferred from one reactor to another. However, if the processing step reduces fuel volume through removal of voids and some solid fission products (e.g. through melting and recasting fuel), then it may be possible to put a larger amount of reused fuel into a given fuel element, increasing the amount of neutron excess obtainable. Conversely, if the presence of solid fission products necessitate putting less fuel into each fuel element, then obtainable neutron excess would decrease. The actual smear density and amount of structure needed for the feed and starter fuel would depend on an analysis of fuel performance as a function of fuel burnup, which has not been performed in this case. Similarly, it would be possible during processing of the fuel to change the coolant volume in an assembly, or even the coolant type. Since this study assumes that the new starter fuel is intermingled in three dimensions with the next generation reactor’s feed fuel, it is assumed that the coolant volume fraction is kept constant to keep operation simple in subsequent generation reactors.

Infinite-reactor doubling times in EFPY are calculated using the same procedure as in Subsection 3.2.1.1, using the power density results in Figure 3. Doubling times are first computed as a function of second-burn (i.e. starter fuel) additional DPA for each of the 52 core compositions shown in Tables 8 and 9. Results for the five different U2Zr-75-T91-30 compositions with different coolant volume fractions are shown in Figure 15. The DPA accumulated over the first burn is determined by the core composition, corresponding to the breakeven burnup that makes the adjusted neutron excess equal to zero. The black Xs in Figure 5.3-7 correspond to a second (starter fuel) burn that results in the same DPA as the first (feed fuel) burn, so burning up to the black Xs or less does not raise the maximum DPA needed per stage.

Figure 15. Infinite-reactor doubling time as a function of second burn DPA for eight U2Zr-75-T91-30 core compositions with different coolant volume fractions

Each of the infinite-medium cases is run to a maximum burnup of 400 MWd/kg, after which the majority of the core compositions have a k_{∞} less than unity, such that burning them further would cost neutrons and raise the doubling time. This is seen in Figure 15 by the fact that most of the curves end shortly after turning upward. Because of the finite DPA simulated, the U2Zr-75-T91-30-Na-225 curve does not have a black X, since it would lie at a DPA higher than the peak burnup simulated.

The results in Figure 15 can be divided into two sets. The cases with a low minimum burnup (coolant volume from 50-150) have minimum doubling times at second-burn DPA levels *higher* than the first-burn DPA (to the right of the Xs). Meanwhile, the cases with coolant volume from 175-225 have doubling time minima that occur at second-burn DPA *lower* than the first-burn DPA (to the left of the Xs), so they can achieve their lowest doubling times without their second stage burns exceeding the DPA limit established by their original equilibrium cycle.

If one takes the DPA accumulated during the first equilibrium cycle burn as the ultimate DPA limit, then the minimum doubling times for the first set are equal to the doubling times at the black Xs, and the minimum doubling times for the second set are the minimum of the curves in Figure 15. Plotting these minimum doubling times for the different T91-structure compositions yields the curves shown in Figure 16. In each of the curves, the leftmost (lowest DPA) points correspond to the lowest coolant volume (50% available fuel volume) equilibrium cycles, with successive points representing increasing coolant volumes. The curves have a characteristic “U”

shape, with low coolant fractions having longer doubling times due to lower power densities, and high coolant fractions having longer doubling times due to worse neutronics. The compositions containing less structure and non-uranium fuel material have higher optimal coolant fractions, equal to roughly 100% of the available fuel volume (the third point from the left) in the U2Zr-75-T91-30 case.

Figure 16. Infinite-reactor doubling time as a function of first-burn DPA for different sodium-cooled core compositions with T91 structure

As shown in Figure 16, using a higher DPA limit to include additional coolant and raise power densities only improves doubling time up to a point, after which degradation in the neutron economy causes doubling times to increase. In this case, there is no reason to have a higher-

DPA *first* burn, but as Figure 15 shows, in certain cases going to a higher DPA for the *second* burn can result in an improvement in doubling time. Figure 17 shows how doubling time can be improved for the U2Zr-75–T91-30 composition by having the second burn go to a higher DPA than the first burn. The figure explicitly shows the amount of coolant present: as the volume of coolant increases, the first burn requires more DPA, and worsening neutronic performance causes doubling time to increase when coolant volume exceeds 125% of the available fuel volume. If one has a higher DPA limit, for example 350 DPA, then the strategy that minimizes doubling time is to use a lower coolant fraction (100% available fuel volume, which corresponds to a coolant volume fraction of 43%), keep the first burn at the same DPA (~265 DPA), and extend the second burn to 350 DPA.

Figure 17. Infinite-reactor doubling time for U2Zr-75–T91-30 core compositions for higher DPA 1st and 2nd burns

The doubling time improvement from having a higher DPA second burn is quite small, for example, for the U2Zr-75–T91-30 composition, increasing the DPA limit from 265 to 365 decreases the infinite-reactor doubling time from 4.22 years to 4.03 years, an improvement of less than 5% for a 100 DPA increase. In the cases with longer doubling times, there is no reason for the second burn DPA to exceed that from the first, since doing so would extend the second burn to the point that the fuel has k_{∞} less than unity, (i.e. past the maximum on Figure 6) meaning that no additional neutron excess is supplied.

One important result from Figures 16 and 17 is that the infinite-reactor doubling times from the no-recladding case (Figure 11) are not improved by breaking up DPA accumulation into two burns. This makes sense because the only difference between the two scenarios is whether DPA continues to be accumulated over the first and second burns. The same sensitivity of doubling time to fuel/structure composition can be seen, with U2Zr performing far better than U10Zr and a lower structure volume performing far better than higher. In the case with recladding, once the DPA limit is high enough to allow the optimal coolant volume fraction, being able to burn to higher DPA yields little extra benefit. This is unlike the case without recladding, in which doubling times continue to get shorter even as the ultimate DPA limit exceeds 500.

Figures 18 and 18 show the corresponding realistic reactor doubling times for T91- and SiC-structure cases with recladding, assuming a radial peaking factor of 2.2, a 90% capacity factor, and a cooling and processing time of 2 years. Intriguingly, the needed DPA limit for optimal doubling times is quite low for the best performing core compositions, on the order of 265 DPA

for T91, or just ~33% higher than the current knowledge limit, and 300 DPA for SiC. Even in the worse performing compositions, ~300 DPA (~325 DPA for SiC) is sufficient to yield the best-case doubling times, with almost no benefit for going past 300 DPA in a single burn. The materials qualification challenge is therefore surprisingly modest for minimum-burnup B&B reactors that use 3D shuffling; much of the challenge lies instead in the engineering of such a system: how to design a high-power-density, axially-segmented core that minimizes structural and fuel alloying materials.

Figure 18. Realistic reactor doubling times as a function of maximum DPA for sodium-cooled core compositions with T91 structure (with recladding)

Figure 19. Realistic reactor doubling times as a function of maximum DPA for sodium-cooled core compositions with SiC structure (with recladding)

3.2.1.3 Effect of cooling time and melt refining

So far the neutron excess from burning feed fuel a second time has been estimated from infinite-medium depletions of unburned feed fuel. This approach is used mainly because it uses the results from the existing infinite-medium model that had been used earlier to estimate the minimum burnup needed for each core composition. This approach does introduce approximations however: first, the composition of the infinite-medium depletion will not exactly match that of fuel discharged from an equilibrium cycle (although it is close), and second, the effect of any post-discharge cooling time or processing is neglected. A better approximation,

and one that would allow the effects of cooling time and processing to be studied, is to perform an infinite-medium depletion of the actual discharge compositions.

Discharged fuel of the core composition with the shortest doubling time (U2Zr-75-T91-30-Na-100) from the convergent infinite slab shuffling case is first allowed to cool for zero years, 2 years, and 20 years. In one set of cases, the composition of the fuel is left unchanged after cooling, while in another a melt refining process occurs which removes volatile and reactive fission products from the fuel mixture. The melt refining process was developed for use with EBR-II and is described in report ANL-6605 [Hesson, 1963]; it is described as removing 100% of noble and volatile fission products (Br, Kr, Rb, Cd, I, Xe, Cs), which escape as gases, and 95% of reactive elements (Sr, Y, Te, Ba, Am, Th, and the rare earths), which react with the zirconia crucibles. It is assumed that the same ratio of original fuel to structure/coolant is maintained, and no additional material (such as makeup uranium) is added. Fuel losses during processing are not modeled, but would be easy to incorporate: for example, 2% process losses would effectively increase the fuel requirement of a reactor by a factor of $1.0/0.98$. Whether the refining process occurs at the end or the middle of the cooling period has little effect on the results obtained.

Figure 20. Fuel k_{∞} vs. additional burnup for different cooling and processing scenarios

Figure 20 shows the fuel k_{∞} as a function of depletion for the different cases considered. The zero year and two year cooling cases are nearly indistinguishable, while the twenty-year cooling cases start with a slightly lower k_{∞} . The k_{∞} decrease in the twenty year case is due to the decay of fissile Pu-241 to Am-241, which occurs with a 14 year half life. The difference in k_{∞} disappears with additional burnup as Am-241 is converted to fissile Am-242m and Pu-241 is burned. Overall, the effect is small because breed-and-burn reactors rely on a fast neutron spectrum that limits absorptions in Pu-239 and Pu-240, which means only a small amount of Pu-241 is generated. This is illustrated for the two best performing core compositions (U2Zr-75-T91-30-Na-100 and U2Zr-75-SiC-30-Na-100) in Figures 21 and 22. The Pu-241/Pu-239 ratio reaches 1.45% in the T-91 case and 1.85% in the SiC case at discharge. The higher ratio in the SiC case is due to a higher discharge burnup and softer spectrum. In worse performing core

compositions the Pu-241 fraction can be higher, but in every case even a decade long cooling time has a minor effect on subsequent k_{∞} .

Figure 20 also shows that the effect of removing fission products during melt refining is tremendous, as the fuel gains a large and lasting increase in its k_{∞} . The net effect of this increase on obtainable neutron excess is shown in Figure 23. The first-burn value of 270 DPA corresponds to an additional burnup of 160 MWd/kgIHM, at which point over three quarters of the available neutron excess has been extracted. At this amount of additional burnup, the cases with a cooling time of twenty years have neutron excesses just 1% below those with zero and two year cooling times, so reactor doubling times would be affected much more by the increased cooling time than by the decreased neutron excess.

Most notably, Figure 23 shows that with melt refining, 24% more neutron excess can be extracted from a piece of used feed fuel. This means that 19% less feed fuel is needed to initiate an equilibrium cycle, which shortens doubling times by approximately 19%. This would reduce the reactor doubling time of the core composition (from Figure 19) from 10 years to approximately 8 years. Not only does a melt-refining process give the ability to reset the fluence limit by starting over with new cladding, it also has the potential to greatly improve used feed fuel and the performance of limited-separations fuel cycles.

**Figure 21. Concentration of plutonium isotopes as a function of burnup from 1D model
(U2Zr-75-T91-30-Na-100 core composition)**

**Figure 22. Concentration of plutonium isotopes as a function of burnup from 1D model
(U2Zr-75-SiC-30-Na-100 core composition)**

Figure 23. Fuel neutron excess vs. additional burnup for different cooling and processing scenarios

3.2.1.4 Effect of fission gas escape

The calculations in the previous three subsections have assumed that all fission products are retained in the fuel and continue to parasitically absorb neutrons through the life of the fuel. In an actual reactor, gaseous fission products would be able to leave the fuel as the fuel is irradiated, either by collecting in a fission gas plenum or by venting from the fuel element completely. This effect is most pronounced for metal fuel, which can develop an open pore structure that allows a significant fraction of fission product gases to escape. Due to the high level of burnup encountered in a B&B reactor (as well as the sensitivity of the B&B neutron economy), it is important to characterize the effect of fission product gas escape.

Fission product gas escape is modeled by assuming that 80% of all noble fission products and fission product decay daughters (Xe and Kr) escape the fuel. This gas escape is assumed to occur exponentially on a 100 second timescale. These values are meant to represent a simple idealized case for metal fuel: the 80% release fraction is a representative value from ANL irradiation tests [Pahl, 1992], and the 100 second timescale is chosen simply so that very short lived noble gases do not escape before decaying. The actual escape fraction and escape lifetime of fission product gases would actually be a function of the fuel temperature and irradiation history, and would depend on both the fuel type (ceramic vs. metal) and the gas species considered (e.g. noble Kr vs. volatile Cs).

The doubling time analyses for a minimum burnup B&B core are repeated for sodium-cooled core compositions with T-91 structure, accounting for fission product gas escape as described. Results are given in Figure 24 for the no-recladding case, and in Figure 25 for the case in which fuel recladding is allowed. The baseline fuel cycle and depleted uranium feed are assumed, with no composition changes during discharge due to cooling or melt refining. Since U_3Si_2 is not a metal fuel, it may not release as much fission gas as is assumed (since assumed values are based on a metal fuel release fraction). Therefore, the effect of fission product escape shown in these figures may be non-conservative for the U_3Si_2 composition.

Figure 24. Infinite reactor doubling times for sodium-cooled compositions with fission product escape (no-reclad case)

Figure 25. Infinite reactor doubling times for sodium-cooled compositions with fission product gas escape (with-reclad case)

Figures 24 and 25 show that fission product gas escape can potentially make a significant difference in fuel cycle performance. In Figure 25, the U2Zr-75-T91-30-Na-100 core

composition (the third point from the left on the bottommost curve) is shifted from its original position of 263 DPA and 4.22 years with no fission product escape (see Figure 18), to 256 DPA and 3.78 years with fission product removal, a 10% decrease in infinite-reactor doubling time. The performance improvement for worse core compositions is more pronounced; for example the U2Zr-75-T91-60-Na-75 core composition is improved from 309 DPA and 10.2 years to 297 DPA and 7.9 years. As with other improvements to neutronic performance, accounting for fission product escape shifts the minimum doubling time composition to higher coolant volume fractions.

The benefits of fission product escape become larger as fuel burnup increases. The reason doubling times improve significantly when fission product escape is accounted for is primarily due to increased neutron excess over the second burn, when feed fuel is reused to start a new equilibrium cycle. This is because the fuel contains more fission products over its second burn so removing a fraction of them makes a larger difference. A similar effect was seen in the previous section by modeling melt refining, which allowed roughly 24% more neutron excess from a given amount of fuel by removing a fraction of the fission products. Note that with fission product escape accounted for, melt refining would yield a smaller improvement, since some of the fission products removed by melt refining would have escaped naturally during fuel irradiation.

3.2.1.5 Effect of natural vs. depleted uranium

All the analyses up to this point have assumed feed fuel compositions containing depleted uranium, consisting of 0.3 atom % U-235 and 99.7% U-238. Using natural uranium (0.71% U-235 by weight) instead of depleted uranium improves the neutronics of B&B reactors, since the additional U-235 is a source of excess neutrons. This leads to a reduction in DPA requirements and a shorter reactor doubling time.

The doubling time analyses for a minimum-burnup B&B reactor were repeated for sodium-cooled core compositions with T-91 structure, replacing the depleted uranium fuel with natural uranium. Infinite-reactor doubling time results are given in Figure 26 for the no-recladding case, and in Figure 27 for the case in which fuel recladding is allowed. The baseline fuel cycle is assumed. Fission product escape from the previous subsection is *not* modeled, so that the individual effects of the two changes can be independently characterized.

Figure 26. Doubling time results for natural-uranium sodium-cooled compositions (no-reclad case)

Figure 27. Doubling time results for natural-uranium sodium-cooled compositions (with-reclad case)

For the U2Zr-75-T91-30-Na-100 core composition (the third point from the left on the lowest line in Figure 26), the results show an infinite-reactor doubling time of 3.96 years for a minimum of 253 DPA. The corresponding numbers for the depleted uranium case are 4.22 years and 263 DPA, so there is a 6% improvement in doubling time by using natural instead of depleted uranium. The improvements for the worse performing core compositions are similarly small, about a 5-10% improvement in doubling time and a 10-15 reduction in necessary DPA. Overall the switch to natural uranium may help improve fuel performance margins but would not by itself make a significant change in fluence requirements or system performance. Similarly, going in the other direction to lower U-235 content depleted uranium tails would not have a large negative effect.

3.2.2 LBE-cooled fast reactor core compositions

As discussed in the core composition overview in Section 3.1, lead and LBE coolant have better infinite-medium neutronic performance than sodium if substituted on a per volume basis, on account of their creating a harder neutron spectrum in the core. LBE performs somewhat better than lead neutronically, so it was selected as the basis of a comprehensive core composition overview using infinite slab models.

The same study of sodium-cooled compositions was repeated for LBE-cooled compositions, with the same combinations of fuel and structure options. The structure to fuel volume ratio was either 30% or 60% of the available fueled volume. Equilibrium cycle simulations were run using

infinite slab models with convergent shuffling, with the discharge burnup corresponding to an infinite-medium average k_{fuel} prediction of 1.02. Tables 10 and 11 show selected characteristics of the 63 different compositions that meet the 1.02 target. Table 10 gives results for compositions with T91 structure, while Table 11 gives results for those with SiC structure.

Compared to the same core compositions using sodium, the LBE cases show lower discharge burnup and shorter axial lengths (so smaller overall dimensions), and correspondingly lower neutron excess and contained burnup requirements. The smaller dimensions of the LBE cases are due to a combination of the lower required burnup as well as the shorter neutron mean free path in the LBE system. The performance of the LBE cooled reactor is less sensitive to the amount of coolant present, and is capable of operating with neutronicallly less robust fuel/structure combinations. Meanwhile, the DPA-to-burnup and fast-fluence-to-burnup ratios are higher in the LBE cases than in sodium, a result of the harder spectrum in the LBE-cooled compositions. Since LBE has less negative impact on neutronics than sodium, optimum core compositions for LBE-cooled systems include a larger coolant fraction than a corresponding sodium-cooled system would.

Table 10. Equilibrium cycle results for LBE-cooled core compositions (T91 structure)

Composition	HM density (g/cc)	Ave. k_{eq}	Discharge burnup fraction	Discharge HT9 DPA	Discharge fast fluence (/cm ²) (>0.1 MeV)	Axial length (cm)	MOEC BU contained (MWy/m ²)	Adjusted ΔN (mol/m ²)
U2Zr-75-T91-30-LBE-50	7.47	1.028	11.4%	256	6.47E+23	197	882	1127
U2Zr-75-T91-30-LBE-75	6.56	1.027	12.1%	265	6.71E+23	210	887	1111
U2Zr-75-T91-30-LBE-100	5.85	1.027	12.9%	274	6.97E+23	223	904	1105
U2Zr-75-T91-30-LBE-125	5.27	1.027	13.6%	283	7.19E+23	237	917	1098
U2Zr-75-T91-30-LBE-150	4.80	1.027	14.3%	292	7.45E+23	248	936	1092
U2Zr-75-T91-30-LBE-175	4.41	1.026	15.2%	302	7.71E+23	259	961	1091
U2Zr-75-T91-30-LBE-200	4.08	1.027	16.0%	313	7.99E+23	267	987	1093
U2Zr-75-T91-30-LBE-225	3.79	1.027	17.0%	324	8.28E+23	278	1023	1092
U10Zr-75-T91-30-LBE-50	6.00	1.027	14.5%	297	7.49E+23	216	1035	1195
U10Zr-75-T91-30-LBE-75	5.27	1.027	15.7%	311	7.87E+23	229	1070	1188
U10Zr-75-T91-30-LBE-100	4.70	1.026	16.9%	326	8.27E+23	245	1109	1183
U10Zr-75-T91-30-LBE-125	4.24	1.026	18.2%	342	8.69E+23	261	1157	1183
U10Zr-75-T91-30-LBE-150	3.86	1.026	19.7%	361	9.17E+23	272	1224	1187
U10Zr-75-T91-30-LBE-175	3.54	1.026	21.4%	383	9.76E+23	288	1318	1196
U10Zr-75-T91-30-LBE-200	3.27	1.025	23.4%	410	1.04E+24	302	1437	1205
U10Zr-75-T91-30-LBE-225	3.04	1.024	26.0%	445	1.13E+24	319	1620	1214
U3Si2-90-T91-30-LBE-50	5.66	1.028	15.5%	292	7.15E+23	220	1072	1208
U3Si2-90-T91-30-LBE-75	4.97	1.027	16.7%	307	7.53E+23	236	1104	1194
U3Si2-90-T91-30-LBE-100	4.43	1.027	18.1%	324	7.96E+23	249	1156	1193
U3Si2-90-T91-30-LBE-125	3.99	1.027	19.7%	342	8.44E+23	265	1223	1194
U3Si2-90-T91-30-LBE-150	3.64	1.027	21.5%	365	9.00E+23	280	1312	1201
U3Si2-90-T91-30-LBE-175	3.34	1.025	23.5%	389	9.62E+23	293	1425	1200
U3Si2-90-T91-30-LBE-200	3.08	1.025	26.2%	424	1.05E+24	309	1605	1211
U2Zr-75-T91-60-LBE-50	6.41	1.027	15.0%	299	7.61E+23	206	1101	1248
U2Zr-75-T91-60-LBE-75	5.72	1.026	16.0%	311	7.91E+23	220	1128	1235
U2Zr-75-T91-60-LBE-100	5.17	1.028	17.1%	325	8.28E+23	231	1164	1237
U2Zr-75-T91-60-LBE-125	4.72	1.026	18.1%	337	8.61E+23	245	1206	1229
U2Zr-75-T91-60-LBE-150	4.34	1.026	19.3%	353	9.01E+23	256	1258	1229
U2Zr-75-T91-60-LBE-175	4.02	1.025	20.6%	368	9.43E+23	267	1317	1228
U2Zr-75-T91-60-LBE-200	3.74	1.026	22.1%	388	9.93E+23	278	1398	1236
U2Zr-75-T91-60-LBE-225	3.49	1.025	23.9%	411	1.05E+24	292	1516	1240
U10Zr-75-T91-60-LBE-50	5.14	1.026	21.7%	387	9.81E+23	232	1566	1387
U10Zr-75-T91-60-LBE-75	4.60	1.025	24.2%	420	1.07E+24	248	1731	1392
U10Zr-75-T91-60-LBE-100	4.15	1.024	27.9%	472	1.20E+24	269	2063	1395
U3Si2-90-T91-60-LBE-50	4.85	1.026	24.0%	396	9.78E+23	237	1729	1412
U3Si2-90-T91-60-LBE-75	4.33	1.025	27.8%	448	1.11E+24	259	2048	1410

Table 11. Equilibrium cycle results for LBE-cooled core compositions (SiC structure)

Composition	HM density (g/cc)	Ave. k_{eq}	Discharge burnup fraction	Discharge SiC DPA	Discharge fast fluence (/cm ²) (>0.1 MeV)	Axial length (cm)	MOEC BU contained (MWy/m ²)	Adjusted ΔN (mol/m ²)
U2Zr-75-SiC-30-LBE-50	7.47	1.028	12.5%	296	5.59E+23	180	912	1123
U2Zr-75-SiC-30-LBE-75	6.56	1.029	13.3%	310	5.85E+23	194	933	1123
U2Zr-75-SiC-30-LBE-100	5.85	1.028	14.1%	323	6.11E+23	205	944	1113
U2Zr-75-SiC-30-LBE-125	5.27	1.028	15.0%	337	6.37E+23	218	969	1113
U2Zr-75-SiC-30-LBE-150	4.80	1.028	15.9%	351	6.63E+23	231	997	1112
U2Zr-75-SiC-30-LBE-175	4.41	1.027	16.8%	366	6.92E+23	241	1021	1108
U2Zr-75-SiC-30-LBE-200	4.08	1.028	17.8%	381	7.20E+23	252	1058	1112
U2Zr-75-SiC-30-LBE-225	3.79	1.027	18.7%	396	7.49E+23	262	1096	1114
U10Zr-75-SiC-30-LBE-50	6.00	1.027	16.4%	347	6.52E+23	198	1113	1210
U10Zr-75-SiC-30-LBE-75	5.27	1.028	17.8%	369	6.93E+23	214	1162	1213
U10Zr-75-SiC-30-LBE-100	4.70	1.027	19.3%	393	7.38E+23	229	1221	1208
U10Zr-75-SiC-30-LBE-125	4.24	1.027	21.0%	420	7.90E+23	243	1304	1218
U10Zr-75-SiC-30-LBE-150	3.86	1.026	22.8%	449	8.44E+23	257	1412	1224
U10Zr-75-SiC-30-LBE-175	3.54	1.025	25.3%	487	9.17E+23	272	1567	1236
U10Zr-75-SiC-30-LBE-200	3.27	1.025	29.1%	550	1.04E+24	291	1858	1249
U3Si2-90-SiC-30-LBE-50	5.66	1.029	16.7%	330	6.18E+23	208	1141	1217
U3Si2-90-SiC-30-LBE-75	4.97	1.028	18.2%	352	6.60E+23	220	1186	1209
U3Si2-90-SiC-30-LBE-100	4.43	1.027	19.9%	376	7.06E+23	236	1256	1210
U3Si2-90-SiC-30-LBE-125	3.99	1.027	21.7%	403	7.58E+23	252	1347	1211
U3Si2-90-SiC-30-LBE-150	3.64	1.026	24.0%	437	8.22E+23	267	1467	1219
U3Si2-90-SiC-30-LBE-175	3.34	1.025	26.7%	478	9.00E+23	284	1691	1230
U2Zr-75-SiC-60-LBE-50	6.41	1.028	18.0%	339	6.27E+23	184	1254	1254
U2Zr-75-SiC-60-LBE-75	5.72	1.029	19.3%	359	6.65E+23	196	1313	1265
U2Zr-75-SiC-60-LBE-100	5.17	1.028	20.7%	380	7.04E+23	209	1375	1262
U2Zr-75-SiC-60-LBE-125	4.72	1.027	22.3%	403	7.48E+23	221	1458	1264
U2Zr-75-SiC-60-LBE-150	4.34	1.026	24.2%	431	8.00E+23	233	1550	1263
U2Zr-75-SiC-60-LBE-175	4.02	1.026	26.7%	468	8.70E+23	249	1763	1277

The better neutronic performance of the LBE core compositions would translate to shorter doubling times than for the sodium cases, if one assumes that the two coolants have the same power density limits. Figure 28 gives the infinite-reactor doubling times for different LBE core compositions as a function of ultimate DPA limit for a case with recladding allowed. The figure assumes the same power density as in the sodium case, and can be directly compared to the sodium results in Figure 16. No fission product escape or melt refining is modeled, and depleted uranium is used as feed fuel.

Figure 28. Infinite-reactor doubling times for different LBE-cooled core compositions (same power density as sodium) (with recladding)

Figure 28 shows that for the same power density limit, the LBE-cooled core compositions are able to achieve shorter asymptotic doubling times than the sodium-cooled compositions, albeit at a higher DPA: the LBE curves are all shifted downward and to the right relative to the sodium ones.

Even though the better neutronic performance of LBE-cooled core compositions leads to shorter doubling times, this result changes when achievable power densities for LBE are taken into account. Figure 29 shows how power densities calculated for LBE compare to those for sodium. The LBE power densities are approximately 60-65% lower, which can be explained by looking

at Equation 1. In both the sodium- and LBE-cooled core compositions, the coolant temperature rise is between ~160K and 180K, corresponding to the difference between the coolant inlet temperature and peak cladding temperature. Meanwhile, LBE has a volumetric heat capacity that is ~40% higher than that of sodium, but a coolant velocity limit that is 75% lower (2 m/s versus 8 m/s for sodium). The net effect is that LBE peak areal power densities are approximately $1.4 \times 0.25 = 35\%$ those of sodium.

The reason for the low 2 m/s velocity limit for LBE is the fact that lead and LBE can both be corrosive to steel at higher velocities, since they are able to strip away steel's protective oxide coating [Samsonov, 1973]. The exact velocity limit depends on the type of steel used and could be raised if an advanced functionally graded composite is used [Short, 2010]. Using a ceramic cladding material such as silicon carbide could also lift this velocity limit and allow much higher power densities.

Figure 29. Comparison of LBE and sodium achievable areal power densities

Figures 30 and 31 show the infinite-reactor doubling times for T91-clad core compositions with and without recladding, incorporating the power density limits from Figure 29. The figures show that the modest doubling time improvements from improved lead or LBE neutronics are completely lost when lower power densities are included: a 20% shorter doubling time vs. sodium due to neutronics in the best case becomes a 150% longer doubling time once power density is factored in. Realistic doubling times would be approximately 2.5 times higher than the infinite-reactor doubling times, on the order of 25 years for the best case core composition. Figures 32 and 33 give the corresponding figures for compositions using SiC structure. These figures assume that the SiC compositions have the same power densities as the earlier T91 compositions; if use of SiC structure allows higher power densities in LBE-cooled systems, then shorter doubling times could be achieved. Without a development such as SiC clad or a

functionally graded composite to raise power densities, LBE/lead-cooled B&B reactors would have significantly longer doubling times than sodium-cooled B&B reactors, despite their better neutronics.

Figure 30. Infinite-reactor doubling times for different LBE-cooled core compositions with T91 structure (no recladding)

Figure 31. Infinite-reactor doubling times for different LBE-cooled core compositions with T91 structure (with re-ladding)

Figure 32. Infinite-reactor doubling times for different LBE-cooled core compositions with SiC structure (no recladding)

Figure 33. Infinite-reactor doubling times for different LBE-cooled core compositions with SiC structure (with recladding)

3.2.3 Helium-cooled fast reactor core compositions

Helium coolant (as well as CO₂ coolant) differs significantly from the liquid metal coolants considered in Sections 3.2.1 and 3.2.2. Being a gas, it is almost completely transparent neutronically, while having much worse heat-transfer characteristics than liquid metals. The

same core composition evaluation study is performed for helium, assuming a helium density of 0.0125 g/cc, corresponding to helium at 20 MPa and 500°C. The thermal hydraulics assumptions result in lower average helium temperatures, but the density of helium modeled has only a very small effect on neutronic performance.

A total of 96 core compositions are tested, with three fuel types, two types of structure in two different amounts, and eight different amounts of coolant. Since the amount of helium has relatively little effect neutronically, coolant volume is varied from 50%–400% of the available fuel volume, instead of 50%–225% for the liquid metal coolants. Of the 96 core compositions, 90 of them can achieve a predicted k_{eq} of 1.02 in the infinite-medium depletion model, and are rerun in a 1D infinite slab model to obtain equilibrium cycle data. Results for these 90 core compositions are presented in Tables 12 and 13, for compositions containing T91 structure and SiC structure respectively.

Table 12. Equilibrium cycle results for helium-cooled core compositions (T91 structure)

Composition	HM density (g/cc)	Ave. k_{eq}	Discharge burnup fraction	Discharge HT9 DPA	Discharge fast fluence (/cm ²) (>0.1 MeV)	Axial length (cm)	MOEC BU contained (MWy/m ²)	Adjusted ΔN (mol/m ²)
U2Zr-75-T91-30-He-50	7.47	1.028	10.1%	236	5.92E+23	225	879	1165
U2Zr-75-T91-30-He-100	5.85	1.028	10.3%	237	5.94E+23	284	890	1175
U2Zr-75-T91-30-He-150	4.80	1.029	10.6%	239	5.97E+23	342	907	1190
U2Zr-75-T91-30-He-200	4.08	1.028	10.8%	240	6.00E+23	401	924	1202
U2Zr-75-T91-30-He-250	3.54	1.029	11.1%	242	6.04E+23	456	943	1219
U2Zr-75-T91-30-He-300	3.13	1.028	11.3%	243	6.05E+23	512	959	1226
U2Zr-75-T91-30-He-350	2.80	1.029	11.6%	244	6.07E+23	569	975	1241
U2Zr-75-T91-30-He-400	2.54	1.029	11.8%	246	6.11E+23	621	990	1248
U10Zr-75-T91-30-He-50	6.00	1.028	12.7%	270	6.75E+23	246	1007	1235
U10Zr-75-T91-30-He-100	4.70	1.027	13.0%	272	6.80E+23	312	1031	1249
U10Zr-75-T91-30-He-150	3.86	1.028	13.4%	274	6.85E+23	378	1062	1271
U10Zr-75-T91-30-He-200	3.27	1.028	13.8%	277	6.90E+23	442	1087	1285
U10Zr-75-T91-30-He-250	2.84	1.028	14.2%	279	6.95E+23	499	1109	1300
U10Zr-75-T91-30-He-300	2.51	1.028	14.6%	282	7.00E+23	562	1140	1314
U10Zr-75-T91-30-He-350	2.25	1.029	15.1%	286	7.09E+23	621	1174	1337
U10Zr-75-T91-30-He-400	2.04	1.029	15.5%	288	7.14E+23	685	1204	1350
U3Si2-90-T91-30-He-50	5.66	1.028	13.2%	262	6.35E+23	255	1033	1248
U3Si2-90-T91-30-He-100	4.43	1.029	13.6%	265	6.41E+23	322	1052	1264
U3Si2-90-T91-30-He-150	3.64	1.029	14.0%	267	6.46E+23	389	1082	1280
U3Si2-90-T91-30-He-200	3.08	1.028	14.4%	269	6.51E+23	454	1107	1293
U3Si2-90-T91-30-He-250	2.68	1.030	14.9%	273	6.58E+23	518	1137	1314
U3Si2-90-T91-30-He-300	2.37	1.029	15.3%	275	6.63E+23	585	1167	1327
U3Si2-90-T91-30-He-350	2.12	1.029	15.7%	278	6.70E+23	648	1199	1344
U3Si2-90-T91-30-He-400	1.92	1.029	16.1%	281	6.75E+23	707	1226	1361
U2Zr-75-T91-60-He-50	6.41	1.028	13.3%	275	6.96E+23	233	1071	1289
U2Zr-75-T91-60-He-100	5.17	1.028	13.7%	277	7.01E+23	285	1093	1301
U2Zr-75-T91-60-He-150	4.34	1.029	14.0%	280	7.06E+23	339	1117	1320
U2Zr-75-T91-60-He-200	3.74	1.028	14.2%	280	7.07E+23	390	1133	1326
U2Zr-75-T91-60-He-250	3.28	1.028	14.6%	283	7.14E+23	442	1162	1337
U2Zr-75-T91-60-He-300	2.92	1.028	14.9%	285	7.17E+23	490	1183	1352
U2Zr-75-T91-60-He-350	2.64	1.029	15.4%	289	7.26E+23	541	1212	1374
U2Zr-75-T91-60-He-400	2.40	1.030	15.7%	292	7.32E+23	589	1241	1387
U10Zr-75-T91-60-He-50	5.14	1.027	18.2%	336	8.50E+23	256	1385	1411
U10Zr-75-T91-60-He-100	4.15	1.027	18.9%	343	8.66E+23	315	1443	1431
U10Zr-75-T91-60-He-150	3.48	1.027	19.5%	348	8.76E+23	372	1497	1447
U10Zr-75-T91-60-He-200	3.00	1.027	20.2%	354	8.91E+23	433	1555	1468
U10Zr-75-T91-60-He-250	2.63	1.027	20.9%	361	9.07E+23	489	1621	1486
U10Zr-75-T91-60-He-300	2.35	1.027	21.7%	369	9.27E+23	548	1699	1510
U10Zr-75-T91-60-He-350	2.12	1.027	22.5%	377	9.44E+23	604	1774	1537
U10Zr-75-T91-60-He-400	1.93	1.027	23.6%	388	9.71E+23	663	1878	1558
U3Si2-90-T91-60-He-50	4.85	1.027	19.4%	335	8.22E+23	265	1458	1426
U3Si2-90-T91-60-He-100	3.92	1.028	20.1%	341	8.36E+23	326	1517	1453
U3Si2-90-T91-60-He-150	3.28	1.028	20.9%	348	8.53E+23	386	1587	1473
U3Si2-90-T91-60-He-200	2.83	1.028	21.7%	356	8.72E+23	447	1660	1491
U3Si2-90-T91-60-He-250	2.48	1.026	22.4%	361	8.84E+23	510	1729	1501
U3Si2-90-T91-60-He-300	2.21	1.027	23.5%	372	9.10E+23	569	1844	1534
U3Si2-90-T91-60-He-350	2.00	1.027	24.4%	382	9.33E+23	630	1932	1557
U3Si2-90-T91-60-He-400	1.82	1.026	25.9%	398	9.71E+23	691	2084	1575

Table 13. Equilibrium cycle results for helium-cooled core compositions (SiC structure)

Composition	HM density (g/cc)	Ave. k_{eq}	Discharge burnup fraction	Discharge SiC DPA	Discharge fast fluence (/cm ²) (>0.1 MeV)	Axial length (cm)	MOEC BU contained (MWy/m ²)	Adjusted ΔN (mol/m ²)
U2Zr-75-SiC-30-He-50	7.47	1.029	11.1%	226	5.00E+23	207	905	1150
U2Zr-75-SiC-30-He-100	5.85	1.030	11.4%	227	5.03E+23	262	920	1169
U2Zr-75-SiC-30-He-150	4.80	1.029	11.6%	229	5.06E+23	315	934	1176
U2Zr-75-SiC-30-He-200	4.08	1.029	11.8%	230	5.09E+23	369	948	1184
U2Zr-75-SiC-30-He-250	3.54	1.029	12.0%	231	5.11E+23	426	959	1198
U2Zr-75-SiC-30-He-300	3.13	1.029	12.3%	233	5.15E+23	476	978	1207
U2Zr-75-SiC-30-He-350	2.80	1.029	12.5%	234	5.17E+23	530	997	1220
U2Zr-75-SiC-30-He-400	2.54	1.030	12.8%	237	5.23E+23	579	1017	1237
U10Zr-75-SiC-30-He-50	6.00	1.028	14.2%	259	5.69E+23	226	1061	1233
U10Zr-75-SiC-30-He-100	4.70	1.028	14.6%	262	5.76E+23	286	1087	1252
U10Zr-75-SiC-30-He-150	3.86	1.030	15.0%	266	5.84E+23	349	1123	1268
U10Zr-75-SiC-30-He-200	3.27	1.029	15.4%	269	5.89E+23	410	1151	1286
U10Zr-75-SiC-30-He-250	2.84	1.029	15.8%	272	5.96E+23	467	1181	1301
U10Zr-75-SiC-30-He-300	2.51	1.028	16.2%	275	6.01E+23	524	1208	1314
U10Zr-75-SiC-30-He-350	2.25	1.029	16.7%	279	6.10E+23	583	1244	1329
U10Zr-75-SiC-30-He-400	2.04	1.029	17.2%	283	6.19E+23	637	1285	1349
U3Si2-90-SiC-30-He-50	5.66	1.029	14.4%	251	5.36E+23	240	1070	1237
U3Si2-90-SiC-30-He-100	4.43	1.029	14.8%	254	5.43E+23	304	1102	1253
U3Si2-90-SiC-30-He-150	3.64	1.029	15.2%	257	5.48E+23	368	1131	1269
U3Si2-90-SiC-30-He-200	3.08	1.030	15.7%	261	5.57E+23	433	1168	1292
U3Si2-90-SiC-30-He-250	2.68	1.029	16.1%	264	5.63E+23	495	1198	1303
U3Si2-90-SiC-30-He-300	2.37	1.030	16.5%	267	5.69E+23	559	1225	1320
U3Si2-90-SiC-30-He-350	2.12	1.029	17.0%	271	5.78E+23	619	1267	1338
U3Si2-90-SiC-30-He-400	1.92	1.030	17.5%	275	5.87E+23	685	1314	1356
U2Zr-75-SiC-60-He-50	6.41	1.028	15.8%	264	5.54E+23	204	1170	1259
U2Zr-75-SiC-60-He-100	5.17	1.029	16.1%	267	5.59E+23	252	1198	1275
U2Zr-75-SiC-60-He-150	4.34	1.029	16.5%	270	5.66E+23	299	1224	1290
U2Zr-75-SiC-60-He-200	3.74	1.029	16.9%	273	5.71E+23	347	1255	1306
U2Zr-75-SiC-60-He-250	3.28	1.029	17.3%	276	5.78E+23	393	1291	1315
U2Zr-75-SiC-60-He-300	2.92	1.029	17.7%	279	5.84E+23	441	1313	1329
U2Zr-75-SiC-60-He-350	2.64	1.028	18.0%	282	5.90E+23	484	1346	1340
U2Zr-75-SiC-60-He-400	2.40	1.029	18.4%	286	5.97E+23	532	1379	1358
U10Zr-75-SiC-60-He-50	5.14	1.027	23.7%	351	7.28E+23	231	1763	1426
U10Zr-75-SiC-60-He-100	4.15	1.026	24.7%	362	7.50E+23	286	1866	1444
U10Zr-75-SiC-60-He-150	3.48	1.027	26.0%	375	7.79E+23	341	1982	1467
U10Zr-75-SiC-60-He-200	3.00	1.026	27.3%	390	8.09E+23	403	2158	1492
U10Zr-75-SiC-60-He-250	2.63	1.025	29.3%	413	8.57E+23	459	2400	1523
U3Si2-90-SiC-60-He-50	4.85	1.027	24.1%	344	7.00E+23	246	1811	1420
U3Si2-90-SiC-60-He-100	3.92	1.026	25.4%	357	7.27E+23	305	1943	1441
U3Si2-90-SiC-60-He-150	3.28	1.026	26.5%	368	7.49E+23	365	2058	1466
U3Si2-90-SiC-60-He-200	2.83	1.026	28.3%	389	7.92E+23	423	2277	1491
U3Si2-90-SiC-60-He-250	2.48	1.025	30.9%	420	8.55E+23	496	2626	1521

The two tables show that increasing the amount of helium coolant present has only a very small effect on minimum burnup. The primary effect of increasing the amount of coolant is to increase the axial length of the system, in some cases increasing it to over six meters. On the other hand, the higher reactivity of the helium-cooled core compositions potentially allows fairly short axial heights (on the order of 2 m) to be viable.

A comparison between calculated peak areal power densities for sodium and helium coolant is shown in Figure 34. Unlike with sodium (or LBE) coolant, the relationship between peak areal power density and coolant volume fraction is no longer linear. This is because the low thermal conductivity of helium causes the temperature difference between the clad and coolant to become much higher, so the coolant temperature rise in Equation 1 is no longer a nearly constant 170K to 180K. Instead, the coolant temperature rises range from ~50K (for cases with high coolant volume fractions) to ~140K. In cases with higher coolant volume fraction, there is less fuel per unit area, and the linear heat rate of the fuel is higher for a given areal power density. The higher linear heat rate causes the temperature difference between the coolant and clad to become the dominant temperature rise, so the coolant temperature rise (and achievable areal power density) go down.

Figure 34. Comparison of helium and sodium achievable areal power densities

The net effect of the tradeoff between coolant temperature rise and the coolant-clad temperature difference is that increasing the amount of coolant only increases power density up to a coolant volume of about 40% (or ~100% of the available fuel volume). Past this point, diminishing returns set in as the coolant-clad temperature difference becomes larger. Keeping the coolant volume fraction low is also desirable for reducing the size and height of the core, as well as increasing the coolant outlet temperature (thus raising thermal efficiency).

There is some spread among the different points in Figure 34 because of the different axial power distributions obtained from the different 1D models. Cases with higher axial peaking (shorter axial lengths) will have higher linear heat rates and higher coolant-clad temperature

differences, resulting in lower coolant temperature rises and lower areal power densities. This effect is more pronounced for gas-cooled systems because of the much higher thermal resistance between the coolant and clad. It is possible to flatten the axial power shape by varying the shuffling sequence used, which can potentially increase areal power densities and reduce doubling times (at the expense of a taller core). Optimizing the axial power distribution would be more appropriately studied with a more detailed thermal hydraulic model, and is not considered in this study.

Based on the power densities in Figure 34, infinite-reactor doubling times are calculated for each of the compositions in Tables 12 and 13 as a function of DPA limit. These infinite-reactor doubling times are given for T91-structure cases without and with recladding in Figures 35 and 36. Figure 35 shows that helium coolant does not allow as short doubling times as sodium, due to the factor of ~2 lower power densities with helium coolant. In Figure 36, the U-shaped curves are all compressed horizontally, because there is almost no change in required DPA when the amount of coolant is increased or decreased. The sharp vertical drop at the beginning of each curve is due to the allowable power density rapidly increasing as the amount of coolant is increased. Increasing the amount of coolant past this point no longer raises power density and gradually degrades neutronic performance, causing the curves to increase again. For helium-cooled systems, doubling times can therefore be minimized by choosing a coolant volume fraction based on thermal-hydraulic performance. Meanwhile, burning starter fuel to a higher DPA on the second burn is able to reduce doubling times to a greater degree with helium than with the other coolants, since the helium-cooled core compositions maintain positive k_{∞} to a higher burnup.

Corresponding infinite-reactor doubling time results for SiC-structure cases are given in Figures 37 and 38. As with the other coolants, switching to SiC results in higher DPA (due to higher SiC DPA cross sections) and longer doubling times due to worse neutronic performance.

One final point regarding gas-cooled fast reactors is that the need for a pressure vessel may make it challenging to implement the large core sizes needed by B&B reactors. In particular, the goal of minimizing doubling times favors larger B&B reactors with flatter radial power distributions, with correspondingly large (>5 m) core diameters that may be challenging to achieve in a gas-cooled reactor.

**Figure 35. Infinite-reactor doubling times for different helium-cooled core compositions
with T91 structure (no recladding)**

**Figure 36. Infinite-reactor doubling times for different helium-cooled core compositions
with T91 structure (with recladding)**

Figure 37. Infinite-reactor doubling times for different helium-cooled core compositions with SiC structure (no recladding)

Figure 38. Infinite-reactor doubling times for different helium-cooled core compositions with SiC structure (with recladding)

4. CONCLUSIONS

One of the primary conclusions of this study is that B&B reactor performance is very sensitive to core composition. In order to minimize required burnup and DPA and achieve short (sub-decade) doubling times, it is likely necessary to develop fuels with low amounts of alloying material and create fuel designs that minimize the amount of structure used.

To highlight this conclusion, Table 14 gives a summary of minimum burnup and DPA results for two types of fuel (U2Zr and U10Zr), two amounts of structure, and two types of coolant (helium

and sodium). The results are taken from the more comprehensive data in Tables 8 and 12. Switching from U2Zr to U10Zr fuel causes minimum burnup to increase by about 30%, and minimum DPA to increase by about 20%, with the increases being larger for the sodium-cooled compositions. Approximately the same effect is seen if the amount of T91 structure is doubled from 30 parts to 60 parts. Including both changes causes minimum burnup and DPA in the helium-cooled core to increase significantly, and causes the sodium cooled core to no longer be able to sustain critical B&B operation.

Table 14. Minimum burnup, DPA, and fast fluence for different core compositions

Fuel type	Relative fuel volume*	Structure type	Relative structure volume*	Coolant type	Relative coolant volume*	Minimum burnup (FIMA)	Minimum HT9 DPA	Minimum fast fluence (/cm ²) (>0.1 MeV)
U2Zr	75	T91	30	He	100	10.3%	237	5.94E+23
U10Zr	75	T91	30	He	100	13.0%	272	6.80E+23
U2Zr	75	T91	60	He	100	13.7%	277	7.01E+23
U10Zr	75	T91	60	He	100	18.9%	343	8.66E+23
U2Zr	75	T91	30	Na	100	13.3%	261	6.27E+23
U10Zr	75	T91	30	Na	100	18.2%	318	7.61E+23
U2Zr	75	T91	60	Na	100	18.2%	320	7.77E+23
U10Zr	75	T91	60	Na	100	N/A	N/A	N/A

*Volumes are in parts, where 100 parts corresponds to the total volume (fuel + gap) inside the cladding

Due to the sensitivity to core composition, only a small number of possible fuel types are attractive for use in low-burnup, short-doubling-time B&B reactors. These are high uranium content metal fuels and a very limited selection of compound fuels, of which U₃Si₂ performs the best (roughly as well as U10Zr). For refractory ceramics, uranium nitride fuel using nitrogen enriched in ¹⁵N performs best, but would require high burnup (>20%) even in a gas-cooled system.

Compared to the type of fuel used, the types of coolant and structure used have a smaller impact on the neutronic performance of a B&B reactor. All the steels considered (HT9, T91, and ODS) exhibit similar performance, with silicon carbide being slightly worse neutronicallly, but with the potential to allow higher power densities. Meanwhile, the amount of structure present does have a strong effect on minimum burnup, more so than the amount of any type of coolant.

Type of coolant used has a relatively minor effect on neutronic performance compared to the type of fuel used. Gas coolants perform the best, followed by lead/LBE then sodium. If the goal is to minimize burnup and fluence experienced by the fuel, one would choose helium coolant, metal fuel, and the type of steel that permits the lowest volume fraction of structure, unless a much smaller volume of silicon carbide can be used. Comparing just liquid metal coolants, lead or LBE require less burnup and DPA than sodium if the volume of coolant is held constant. However, since sodium allows higher power densities than lead or LBE, a smaller volume of sodium is needed to achieve a given power density, so sodium actually has less impact on minimum burnup and DPA than lead/LBE for the same cooling performance.

The values given in Table 14 are likely to be bounding values for the amount of burnup and DPA required for a minimum-burnup B&B reactor design. The fuel in such a design is likely to have a composition between U2Zr and U10Zr, and the amount of structure would likely be between 30% and 60% of the available fuel volume. The values show that the levels of burnup required

are less than 20%, within current knowledge limits, and the required amount of DPA range from about 240 to 350.

The exact burnup and DPA requirements for an actual system would depend on the fuel, coolant, and structure fractions of an explicit fuel design. The fuel design would need to be capable of sustaining the required amount of fuel burnup and DPA, so consistent neutronic and fuel performance calculations need to be performed for the design. For example, if one increases the thickness of fuel cladding to raise the burnup limit of a fuel design, this would also increase the minimum burnup required for B&B operation.

If fuel performance data show that burnup and DPA limits are sufficient for using sodium coolant, then using sodium instead of a gas coolant would allow a large increase in power density, potentially improving the economics of a B&B system. Because it takes into account both neutronic and thermal hydraulic performance, reactor doubling time can be used as a comprehensive figure of merit for comparing different core composition options.

In terms of minimizing doubling time, the thermal hydraulic performance of the different coolants is more significant than the differences in neutronic performance. With liquid metal coolants, there is a tradeoff between neutronic and thermal hydraulic performance: higher coolant volume fractions allow higher power densities but worsen neutronic performance by softening the neutron spectrum and causing more parasitic absorptions. The optimal sodium

coolant volume fraction is on the order of 40%, which corresponds well to conventional sodium fast reactor designs (from Figure 3). The optimal lead/LBE coolant volume fraction is greater because lead/LBE coolant produces a harder neutron spectrum. In both liquid metal cases, fuel/structure combinations with worse neutronic performance would reduce the optimal coolant volume fraction. Gas coolants are neutronic nearly transparent, so the coolant volume fraction can be optimized on the basis of thermal hydraulics alone.

This study computes infinite-reactor doubling times for different core composition options in a number of scenarios. Among the different coolants, sodium can achieve the shortest doubling times, followed by helium/CO₂ then LBE/lead, following the order of their calculated thermal hydraulic performance. Without any recladding, minimizing doubling time requires being able to burn fuel up to approximately 500 DPA. Having the option to reclad fuel between reactor passes allows near-minimum doubling times to be achieved at much more modest levels (<300) of DPA. Fission product escape has a small but non-negligible effect on neutronic performance, and is important to model in a B&B reactor. The choice of natural versus depleted uranium for feed fuel makes less of a difference. Fuel composition changes due to a processing step such as melt refining can potentially make a large (~20%) impact on doubling times, if a significant fraction of fission products is removed during processing.

To summarize the important doubling time conclusions from this study, realistic reactor doubling times for different coolants using T91 structure as a function of DPA with recladding are shown in Figures 39 through 41. In these results, no fission product removal or composition changes

due to processing are assumed. The baseline limited-separations fuel cycle is used, and peak power densities are the ones shown in Figures 3, 29, and 34. Other important assumptions are listed on the figures. The figures show that to have a reasonable chance at achieving sub-decade doubling times, it would be either necessary to use high power density sodium cooled B&B reactors, or raise the power density limits of the LBE- or gas- cooled reactors. Meanwhile, with some form of fuel recladding process and the ability to qualify reclad fuel, it would be possible to create a fleet of B&B reactors that could expand itself with no additional fissile input, all with a fairly modest (<350) DPA requirement.

Figure 39. Realistic reactor doubling times as a function of maximum DPA for sodium-cooled core compositions with T91 structure (with recladding)

Figure 40. Realistic reactor doubling times as a function of maximum DPA for LBE-cooled core compositions with T91 structure (with recladding)

Figure 41. Realistic reactor doubling times as a function of maximum DPA for helium-cooled core compositions with T91 structure (with recladding)