Reactivity Coefficients for Use in UMLRR Transient Analyses: Quantitative Analysis and Partial Validation via Comparison with Actual Reactor Data

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Introduction/Overview

A variety of studies are currently on-going to support the re-licensing of the UMass-Lowell research reactor (UMLRR). In particular, an important component of the safety analyses involves the study of a series of PARET[1] reactivity-induced and flow-induced transients that model various accident scenarios within the UMLRR. An important set of input parameters needed in these analyses are the fuel temperature, coolant temperature, and coolant void reactivity coefficients that are used to model the inherent reactivity feedbacks within the actual system. These reactivity coefficients are usually computed using a variety of steady state reactor physics codes, and then the resultant coefficients are validated to the extent possible through actual measurements within the real system. However, the validation process is usually via indirect means, since it is difficult (or impossible) in most cases to directly measure the individual reactivity coefficients of interest.

This report summarizes our efforts to establish a reasonable set of reactivity coefficients for the UMLRR for use within subsequent PARET transient analyses. A full set of reactivity coefficients (α_{Tf}, α_{Tm}, and α_V for the fuel temperature, moderator temperature, and moderator void, respectively) was determined by Michael Pike as part of his MS Thesis.[2] Here we briefly summarize his work and preliminary results, and then re-analyze his reference data to obtain a slightly modified set of coefficients for use in subsequent PARET code calculations. Then, a series of relatively mild reactivity and flow transients were performed within the reactor and modeled in PARET so that we could validate the PARET model, with focus, of course, on the accuracy of the computed reactivity coefficients. The results of this comparison study show that the PARET simulations, with a new set of reactivity coefficients based on the work performed in Ref. 2, do a reasonably good job at modeling actual reactor behavior under a variety of situations. Thus, future accident analyses with PARET will utilize the base PARET models and the numerical values for α_{Tf}, α_{Tm}, and α_V documented here.

General Treatment of Reactivity Coefficients

Each reactivity coefficient is defined in a similar fashion. For a temperature effect, for example, we simply write the temperature coefficient of reactivity as

$$\alpha_T = \frac{\partial \rho}{\partial T} \tag{1}$$

where the temperature might be associated with the fuel, coolant, or structural materials. Since $\rho = (k - 1)/k$, this can be written as

$$\alpha_T = \frac{\partial \rho}{\partial T} = \frac{\partial}{\partial T}\left(1 - \frac{1}{k}\right) = \frac{1}{k^2}\frac{\partial k}{\partial T} \approx \frac{1}{k}\frac{\partial k}{\partial T} \tag{2}$$

where the last approximation ($k^2 \approx k$) is valid for a near-critical or critical reference state (i.e. $k_{ref} \approx 1.000$). And, from the basic definition of a reactivity coefficient, α_T, we see that the feedback reactivity can be written as

$$\rho_f(t) = \Delta\rho(t) \approx \frac{\partial \rho}{\partial T}\Delta T(t) = \alpha_T\left\{T(t) - T_{ref}\right\} \tag{3}$$

Thus, once the reactivity coefficients are known, they can be used to approximate the inherent feedback reactivity within the system [as implied by eqn. (3)].

In practice, the temperature coefficients (with units of $\Delta k/k$ per unit temperature) are not really very easy to quantify. Often these are computed using sophisticated computer codes that attempt to model the reactor configuration in as much detail as possible. Usually two discrete temperatures are chosen and the representative cross sections and atom densities are determined for each temperature. The neutron balance equation is then solved using these data sets to obtain two values of k_{eff}. For example, given the T-k combinations,

T_1 = reference temperature $\rightarrow$ k_1 = reference k_{eff}

T_2 = perturbed temperature $\rightarrow$ k_2 = perturbed k_{eff}

the average temperature coefficient over the given temperature range is

$$\bar{\alpha}_T = \frac{\int_{T_1}^{T_2} \alpha_T(T)\,dT}{\int_{T_1}^{T_2} dT} = \frac{\int_{T_1}^{T_2} \frac{1}{k}\frac{\partial k}{\partial T}\,dT}{T_2 - T_1} = \frac{1}{T_2 - T_1}\int_{T_1}^{T_2}\frac{dk}{k} = \frac{\ln\left(k_2/k_1\right)}{T_2 - T_1} \tag{4}$$

As implied here, the temperature coefficient is a function of temperature, so eqn. (4) may be evaluated over several T-k pairs to develop the rough behavior of $\bar{\alpha}_T$ vs. temperature. As an alternative approach, one can also plot $\Delta k/k$ vs. T for a set of discrete T-k pairs and, via a curve fit or a finite difference approximation, form $\bar{\alpha}_T$ vs. T from the basic definition given in eqn. (2).

It should be noted that the moderator temperature coefficient is often broken into two terms: one that accounts for the spectral and cross section changes that accompany a change in moderator or coolant temperature, and one that accounts for the change in the physical density of the coolant. Thus, for the moderator,

$$\alpha_{MTC} = \alpha_{Tm} + \alpha_{\rho m} \tag{5}$$

where α_{MTC} is the total moderator temperature coefficient, α_{Tm} is the temperature component, and $\alpha_{\rho m}$ represents the effect on k_{eff} of a change in the physical density due to a temperature change. These effects can be evaluated separately in the reactor physics codes by independently changing only the moderator temperature or the coolant density during the cross section processing and core calculation steps. Once the density component has been isolated, we can compute the void coefficient, using

$$\alpha_V = \frac{\upsilon}{k}\frac{dk}{d\upsilon} = \frac{\upsilon}{k}\frac{dk}{dT}\frac{dT}{d\upsilon} = \upsilon\left(\frac{1}{k}\frac{dk}{dT}\right)\Bigg|_{\text{mod}\,\rho}\frac{dT}{d\upsilon} = \upsilon\alpha_{\rho m}\frac{dT}{d\upsilon} \qquad (6)$$

where υ refers to the specific volume and $dT/d\upsilon$ can be obtained from basic thermodynamic data for the moderator (i.e. ordinary water in the case of interest here). Also, since the actual fluid volume, V, and mass, m, are related by $V = \upsilon m$, eqn. (6) can also be written as

$$\alpha_V = \upsilon m\alpha_{\rho m}\frac{1}{m}\frac{dT}{d\upsilon} = V\alpha_{\rho m}\frac{dT}{dV} \qquad (7)$$

One should also note that the void coefficient, α_V, is almost always given and used with units of $\Delta k/k$ per %void, so eqn. (7) is often divided by 100 to give the %void notation.

In PARET, the reactivity components are entered in units of dollars ($) instead of $\Delta k/k$, and this is obtained by division by β_{eff}. Thus, the feedback reactivity, in dollars, is given as

$$\rho_f(t) = \alpha_{Tf}\left\{T_f(t) - T_{f_{ref}}\right\} + \alpha_{Tm}\left\{T_m(t) - T_{m_{ref}}\right\} + 100\alpha_V\left\{V_m(t) - V_{m_{ref}}\right\} \qquad (8)$$

where the coefficients are given in either $/$^oC$ or $/%void. More complicated expressions are available to account for the temperature dependence of the reactivity coefficients, but the current PARET models of the UMLRR assume that these are relatively constant with temperature (see discussion below), so this additional detail is not used at present.

Reactivity Coefficients for the UMLRR

Following the above computational outline, an attempt has been made to compute accurate reactivity coefficients for the UMass-Lowell research reactor (UMLRR). During the early UMLRR HEU to LEU conversion effort, Argonne National Laboratory (ANL) computed these quantities for the UMLRR (see Ref. 3) and these values have been used as "reference" for the last 20+ years. However, the current UMLRR core configuration is quite different from the one used by ANL, and clearly we should have the base computational capability to do these type of calculations locally at UMass-Lowell. Thus, as part of his MS Thesis[2], Michael Pike recently computed a series of reactivity coefficients for the current M-2-5 core configuration (he used a BOL model since we expect that the small amount of burnup will not have much effect on these coefficients). Most of the calculations were done using 2-group theory within our standard 3-D VENTURE[4] model of the UMLRR. The cross sections for each temperature considered were generated by a full cross section processing sequence in SCALE[5] using several different modules to account for both resonance and spatial self shielding effects and to compute the proper fine-group flux weighting spectrum for collapse to two groups. Some energy group sensitivity studies were performed to show that the use of 2-groups was sufficient to get reasonable results. Similarly, a 2-D vs. 3-D comparison showed some slight differences due to axial leakage affects, so all the final results were reported with the 3-D geometry.

The results from the computer calculations by Michael Pike and the "reference" ANL values are given in Table 1. These reactivity coefficients were generated near room temperature conditions using eqn. (4) with a 10 ^{o}C temperature interval. The single combined $\alpha_{Ttot} = \alpha_{ITC}$ from a pool cooldown run within the UMLRR performed in January 2013 is also included.[6] Comparing this measured value of the total or isothermal temperature coefficient (ITC) for the M-2-5 core at about 50-55 MWD and the calculated value at BOL shows that they differ by nearly a factor of

two. As noted above, we certainly acknowledge that these reactivity coefficients are difficult to compute and to measure, so some differences were expected here -- but not a factor of two -- since this kind of difference can have a significant effect on the transient response of the system with feedbacks included! Thus, before starting a series of PARET transient simulations, it became apparent that we needed to evaluate the adequacy of the calculated reactivity coefficients in more detail.

Table 1 Reactivity coefficients for several UMLRR models (Δk/k/^{o}C) (from Ref. 2)

Component	M-1-3 (BOL)	**M-2-5 (BOL)**	M-2-5 WPI Fuel	ANL Data	**M-2-5 Measured**
Water Temp Only	-4.4e-5	-4.2e-5	-1.3e-5	-4.8e-5	---
Water Density Only	-5.3e-5	-5.8e-5	-5.3e-5	-4.6e-5	---
total $T_{coolant}$	-9.7e-5	-1.0e-4	-6.6e-5	-9.4e-5	---
T_{fuel}	-1.7e-5	-1.7e-5	-1.7e-5	-1.5e-5	---
$T_{coolant}$ + T_{fuel}	**-1.1e-4**	**-1.2e-4**	**-8.3e-5**	**-1.1e-4**	**-5.9e-5**
void (Δk/k/%void)	-2.3e-3	-2.6e-3	-2.3e-3	-2.4e-3	---

To address the inconsistent calculated vs. measured isothermal temperature coefficient -- that is **-1.2e-4 Δk/k/^{o}C vs. -5.9e-5 Δk/k/^{o}C** -- the first task involved a detailed review of the work done in Refs. 2 and 6 (i.e. Michael Pike's work and the analysis of the Pool Cooldown Experiment from Jan. 2013). Both analyses seemed correct and self-consistent but, during the re-analysis of Michael Pike's raw data, it was decided to quote values for the coefficients using the linear curve fits evaluated at 24 ^{o}C ≈ 75 ^{o}F instead of just using the first data point as done in Ref. 2.

In particular, using the reference and perturbed raw k_{eff} values directly from Michael Pike's work, the temperature coefficients vs. temperature for the different components (fuel temperature, coolant temperature, and coolant density) were computed using eqn. (4), and a summary of the results, along with a series of linear curve fits, are given in Fig. 1. From here we see that the fuel and temperature-only moderator components are nearly independent of temperature, and that the temperature dependence of the moderator density contribution is what drives most of the temperature dependence of the total or isothermal temperature coefficient, α_{ITC}. However, if the density component is converted to a void coefficient via eqn. (6), one sees that the void coefficient, α_V, is also a slowly varying function of temperature with a value near -2.6e-3 Δk/k per %void, as seen in Fig. 2. This occurs because the dT/dυ term decreases at roughly the same rate as $\alpha_{\rho m}$ increases (in the negative direction). Thus, for the PARET model, only single temperature-independent values of α_{Tf}, α_{Tm}, and α_V are needed. For consistency, the linear curve fits were evaluated at 24 ^{o}C to obtain the desired (constant) reactivity coefficients for the PARET studies. The resultant values are summarized in Table 2 along with the corresponding results quoted in Ref. 2 (and in Table 1 given above) for ease of comparison.

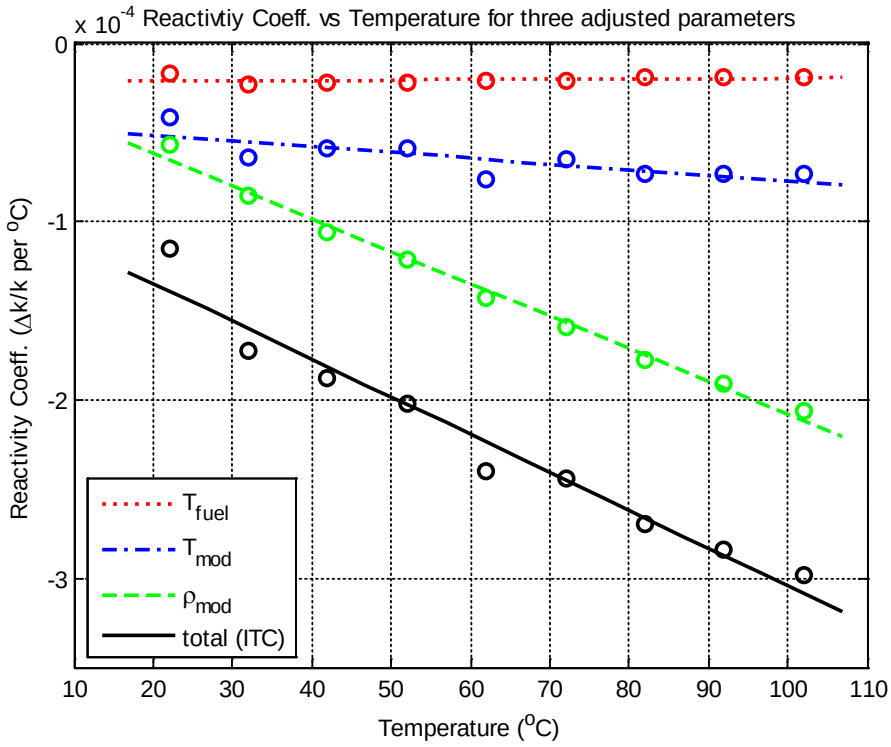


Fig. 1 Calculated reactivity coefficients vs. temperature for the M-2-5 BOL core.

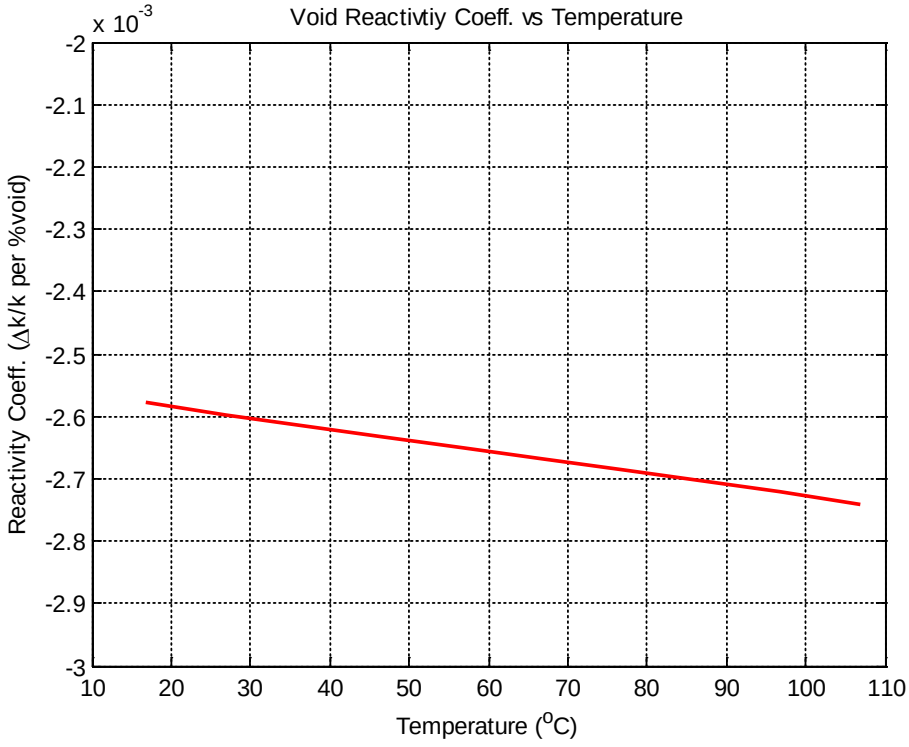


Fig. 2 Calculated void reactivity coefficient vs. temperature for the M-2-5 BOL core.

Table 2 New reactivity coefficient evaluation (Δk/k/°C) for the M-2-5 BOL core.

Component	**Original Data from Ref. 2**	**New Evaluation**	**M-2-5 Measured**
Water Temp Only	-4.2e-5	-5.30e-5	---
Water Density Only	-5.8e-5	-6.91e-5	---
total $T_{coolant}$	-1.0e-4	-1.22e-4	---
T_{fuel}	-1.7e-5	-2.12e-5	---
$T_{coolant}$ + T_{fuel}	**-1.2e-4**	**-1.43e-4**	**-5.9e-5**
void (Δk/k/%void)	-2.6e-3	-2.59e-3	---

As apparent from the tabulated data and from a close look at Fig. 1, we see that the new evaluation values, in most cases, are a bit more negative than those reported in Ref. 2. But, in particular, the re-evaluation preformed here has not shed any light on the discrepancy between the calculated and measured value of the ITC -- in fact, the difference got a little larger! To show this difference visually, the temperature-dependent computed and measured ITC are plotted in Fig. 3, where we see that the measured values were only available over a narrow 10 °C interval and, for use in subsequent work, these were extrapolated to the same interval associated with the computed data. Thus, the inconsistency originally reported in Ref. 2 remains -- so the only conclusion here is that more experimental data (possibly of a different nature) are needed!

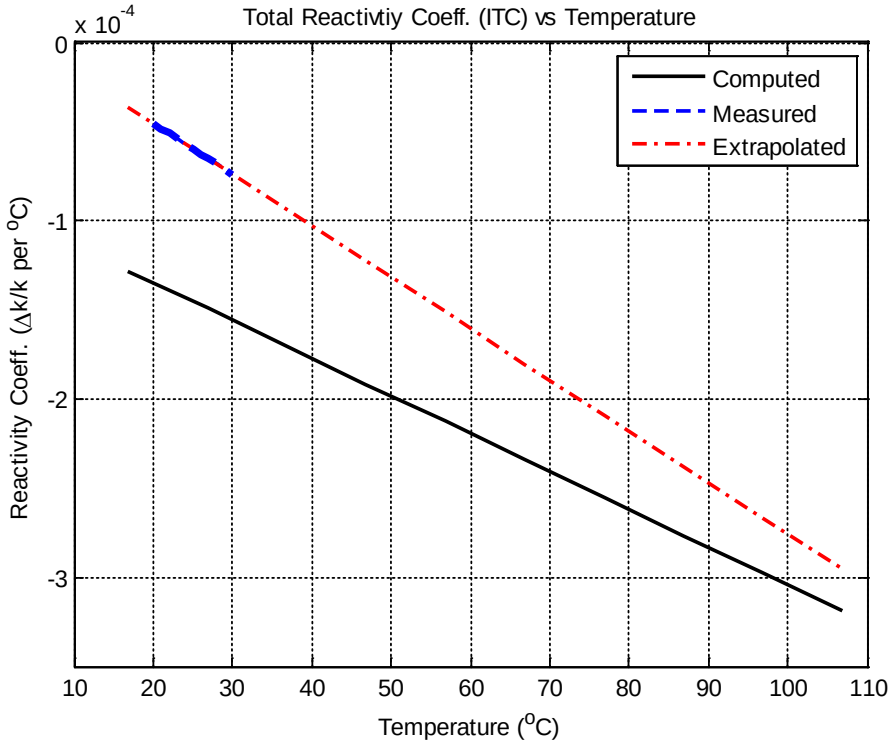


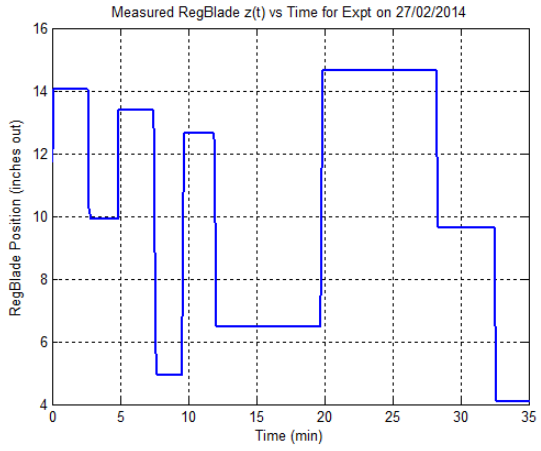
Fig. 3 Calculated and measured isothermal temperature coefficients for the M-2-5 core.

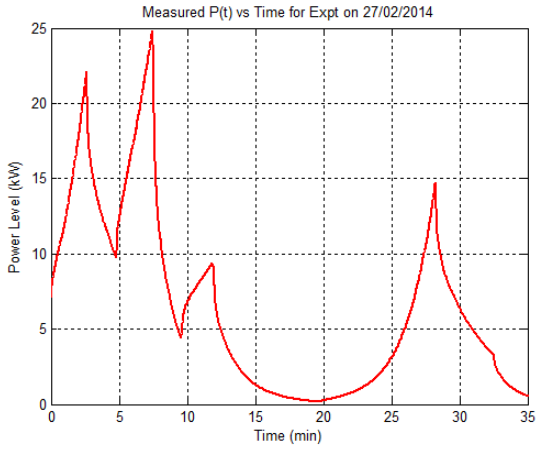
Operational Test Sequences within the UMLRR

An alternative to comparing computed and measured reactivity coefficients directly is to evaluate the adequacy of the computed values by using them in the simulation of a variety of actual reactor transient scenarios. This approach also has the added benefit of validating the PARET simulation model as part of the overall benchmark evaluation. Thus, a series of operational tests, including both reactivity-driven and flow-induced transients, were performed within the UMLRR and these same sequences were also modeled and simulated within the PARET code using the "new evaluation" reactivity coefficients listed in Table 2.

In particular, four reactivity-induced cases were evaluated that involved both forced and natural convection flow situations, as follows:

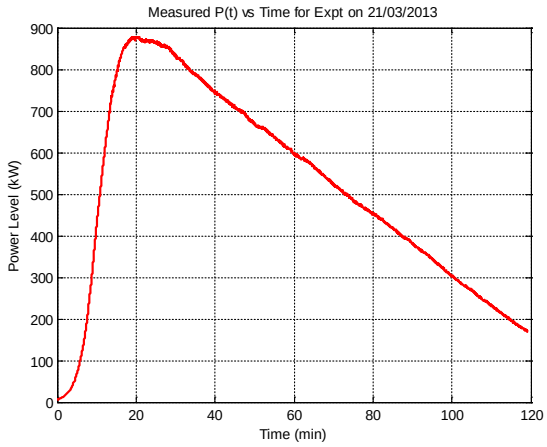
Reactivity Test #0: This reactor sequence was run on February 27, 2014. The reactor was operating in forced-flow mode at about 1650 gpm. The initial power was about 7.1 kW and the inlet temperature was about 21.6 ^{o}C when a series of RegBlade movements was initiated. The magnitude and timing of the blade movements were such that the reactor power remained relatively low ($\leq$ 25 kW) throughout the reactor run. A high flow rate combined with low power operation represents a "zero power" system, where both temperature feedbacks and xenon reactivity should be negligible. The actual blade movements and the resultant measured power profile are shown in the sketches below. The observed P(t) behavior is qualitatively as expected considering the given manual movement of the RegBlade -- that is the power increases when the blade is withdrawn and it begins to decrease when it is inserted. This feedback-free case was run as a test of the basic PARET simulation model and of the kinetics parameters (the effective delayed neutron fraction, β_{eff}, the generation time, Λ, the precursor decay constants, λ_i, etc.) used to model the UMLRR system via point kinetics. A good comparison of the PARET simulation data and measured results for this test is essential to establish an accurate feedback-free model so that further testing with feedbacks can be performed. This case is referred to as "Test #0" since it is the foundation for all the remaining comparisons.



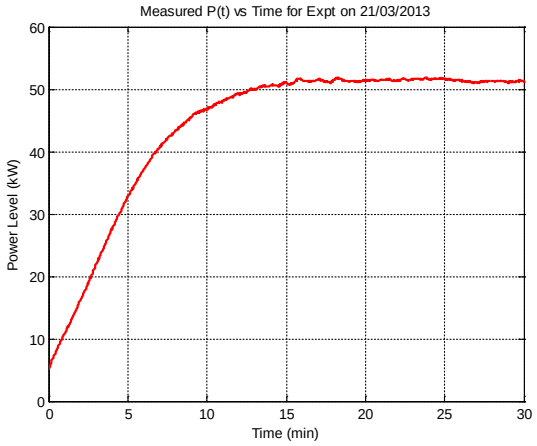


Reactivity Test #1: This reactor sequence was run on March 21, 2013. The reactor was operating in forced-flow mode at about 1650 gpm. The initial power was about 5 kW and the inlet temperature was about 24 ^{o}C when the RegBlade was ramped out 2.7 inches, and then the system was left to stabilize itself without additional reactivity control by the operators. The

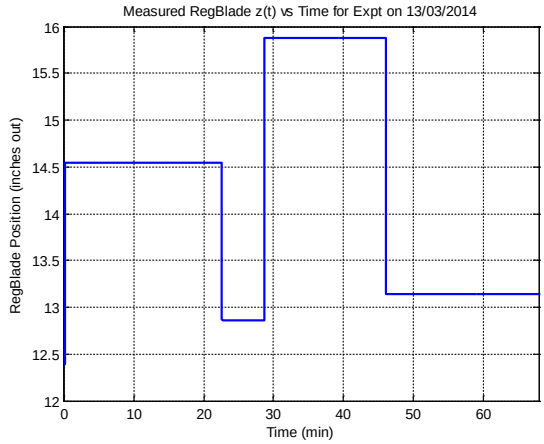
RegBlade movement lasted about 5 seconds and, from the blade worths curves, we know that the resultant reactivity insertion was about 0.062 %Δk/k. The added positive reactivity causes the power level to initially increase, but after a short time, the rate of increase slows due to the inherent negative feedback reactivity that tends to offset the initial positive insertion associated with the RegBlade movement. Eventually, the reactor power starts to level off and then decreases as xenon builds up due to high power operation above 100 kW. The measured power vs. time profile for this reactor run is shown in the plot on the right. However, PARET does not have a built-in xenon model, so for comparison purposes, we can only use the first few minutes of this sequence since xenon feedback starts to become important after this point.

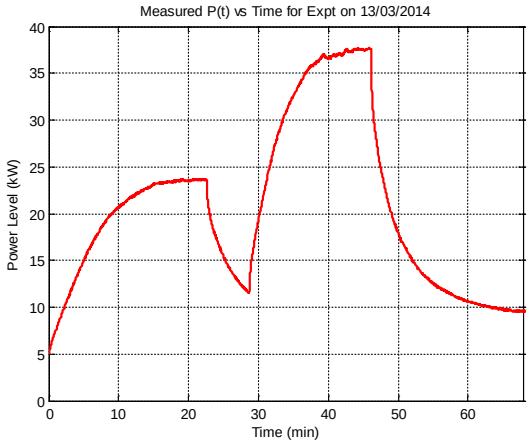


Reactivity Test #2: This reactor sequence was also run on March 21, 2013, but this time the reactor was operating in natural convection mode. Again, the initial power was about 5 kW and the inlet temperature was about 24 ^{o}C. The RegBlade was ramped out about 3.6 inches to add about 0.080 %Δk/k positive reactivity to the system in about 6 seconds, and then the reactor was left to stabilize itself without any control by the operators. As in Test #1, the temperature feedbacks quickly tend to negate the initial positive reactivity insertion, and the reactor tends to stabilize at a new power level where the feedback exactly offsets the initial positive insertion. In natural convection mode, however, where the maximum power level is 100 kW, xenon has little effect, so the power level actually approaches a constant value as seen in the P(t) vs. time plot for this reactor run. For Test #2 the core temperatures are larger than in Test #1 because of the reduced coolant flow rate in natural convection mode, so the temperature feedback is stronger and the resultant power increase is significantly less than observed for the forced flow case.



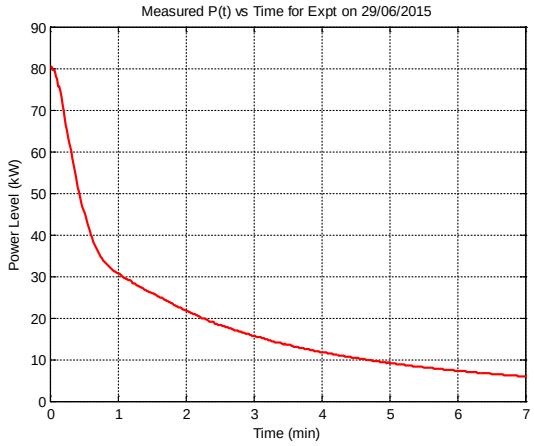
Reactivity Test #3: This natural convection case was run on March 13, 2014. It involved similar starting conditions as Test #2, but this time four RegBlade movements, two withdrawals and two insertions, were made over nearly 70 minutes of operation. The actual blade movements and the resultant measured power profile are shown below. The observed P(t) behavior is qualitatively as expected considering the given manual movement of the RegBlade and the inherent negative reactivity feedback observed in the previous tests. This test is certainly similar to Test #2, but it represents a more realistic operational scenario for testing the PARET code (unfortunately a similar operational sequence and PARET comparison for forced flow mode cannot be done since PARET does not have xenon simulation capability).





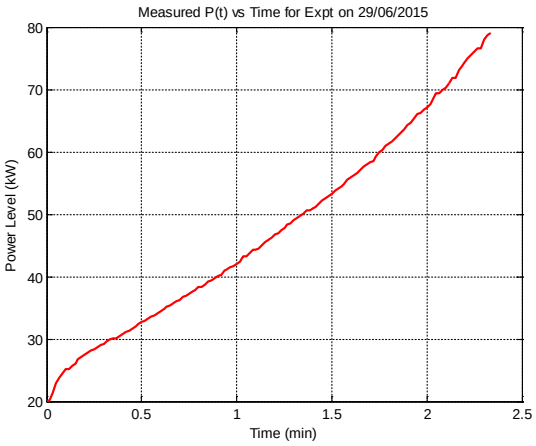
In addition to the reactivity transients, a series of pump-off and pump-on flow transient tests were also performed in June 2015. Two tests of each type were performed at two different power levels. However, the details from only one test of each type are discussed here, since the reactor behavior for different starting power levels are similar, differing only in the magnitude of the observed response. The two cases to be described here are as follows:

Pump-Off Test: This sequence was performed on June 29, 2015. The reactor was operating at steady state at about P = 80 kW with the primary pump on with a flow rate of about 1700 gpm down through the core. To initiate the transient, the primary pump was turned off with the RegBlade in manual mode. No operator interaction occurred after the pump was turned off. The expected scenario after a pump-off event is for the pump to coast down exponentially to a stop in about 10 seconds, for the core temperatures to increase due the reduced flow rate and, eventually, for the flow to reverse direction and flow up through the core as natural convection is established. With the temperature increase and negative inherent feedback, negative reactivity will be inserted as an intrinsic aspect of the pump-off event, and cause the power level to decrease -- just as seen in the measured P(t) vs. time plot shown here. The change in slope of the decreasing P(t) curve just before the 1 minute mark is associated with the flow reversal and establishment of an upward-directed natural convection flow. Once established, the free convection up-flow tends to cool the core somewhat, which adds some positive reactivity and tends to slow down the rate of power reduction (i.e. it now has less negative reactivity).



Pump-On Test: This reactor sequence was also performed on June 29, 2015. The reactor was operating at steady state at 20 kW in natural convection mode when the pump was turned on. In natural convection mode the flow rate is up through the core at only 1-2 cm/s. At this flow rate, even low power levels produce a significant temperature rise in the core. Once the pump is turned on, the warm water in the core is rapidly replaced with the cooler pool water from above the core -- this is often referred as a “cold water insertion event”. With negative feedback and a

rapid decrease in core temperature, the pump-on event rapidly inserts positive reactivity into the system, with a subsequent rise in power over time, as seen in the measured P(t) plot. The transient occurs very quickly since the pump speed approaches full capacity in only a few seconds. Also, since the UMLRR limit in natural convection mode is 100 kW (and this sequence was started in this operational mode), the transient needs to be terminated by operator control before the 100 kW limit is reached -- thus, the overall sequence takes place in only 2-3 minutes.



To simulate the pump-off and pump-on sequences in PARET, we need to have information concerning the pump coast down and startup curves. Back in 2011, a similar series of tests were performed and the so-called "pumps curves" were analyzed in some detail. In particular, for a pump-on event, the pump approaches full capacity very rapidly (in about 2 seconds) and this was modeled as a ramp that goes from zero to full flow in 2 seconds. For the pump-off scenario, the pump coast down is more gradual, occurring over a period of about 10 seconds. On March 3, 2011, a series of four pump-off events were recorded and, from the measured flow rate data, an average coast-down curve was developed as shown in Fig. 4. Numerical data from this curve was extracted at 1-second intervals and used within PARET to represent the fraction of full flow following a pump trip, with the value set to exactly zero for $t \geq 10$ s.

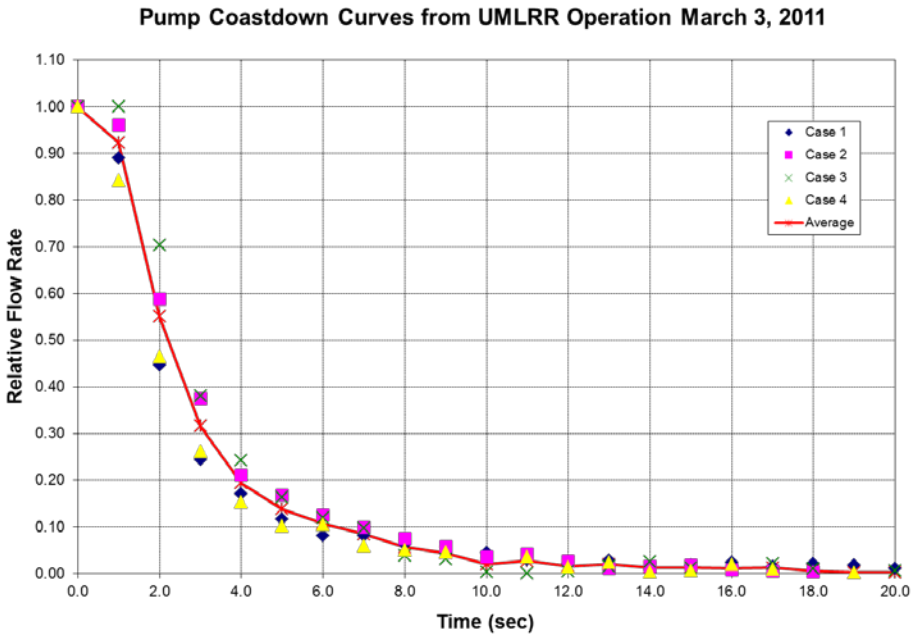


Fig. 4 Pump coast-down curve for the UMLRR primary pump.

Comparison of PARET Simulations with Measured Operational Data

Each of the above reactor sequences was simulated in PARET. In each case, the power vs. time profiles were the primary quantity of interest. However, the measured P(t) signal (average of the Linear Power 1 and 2 signals) is also used within an inverse kinetics routine to obtain measured total ρ(t) data (see Ref. 7), which includes both the external reactivity and inherent temperature feedback reactivities -- and this quantity is also of importance because of our current interest in evaluating the adequacy of the reactivity coefficients used within the PARET simulations. Within this context, a short MATLAB code was written to extract the output data from the PARET output files and to construct computed ρ(t) profiles for the individual components and total reactivity, so that these can be compared directly to the measured result. Additional time-dependent computed results, such as various temperatures and flow rates, are also available to assist in understanding various transients. These computed quantities are useful for analysis purposes, even though corresponding measured data are not available.

Concerning results from the calculational vs. experimental comparisons, Fig. 5 contains the primary comparison for the Test #0 feedback-free case. At first glance, the two P(t) profiles compare well, with a slight under prediction during the first 10 minutes of operation and a slight over-prediction of P(t) for the last 20-25 minutes. Also, as apparent in the right half of the figure, the total PARET reactivity follows the measured results accurately, with negligible temperature or void feedback reactivity (as designed).

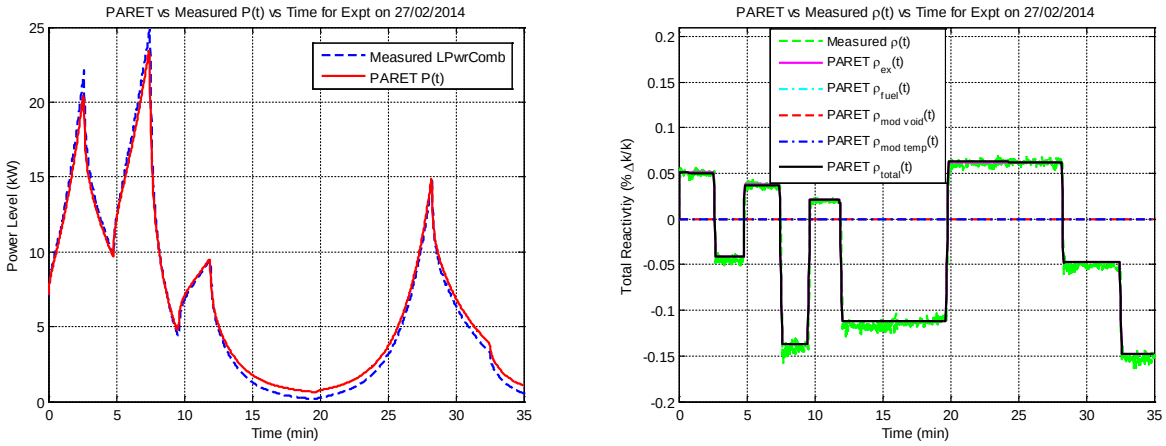


Fig. 5 Summary PARET results for the feedback-free reference test.

However, previous work[8] with the experiment performed on Feb. 27, 2014 showed that a simple feedback-free Matlab-based point kinetics model written and tested as part of a Reactor Experiments course at UMass-Lowell did an even better job at matching the measured results for this experiment, as seen in Fig. 6. After some investigative work, the slight differences seen in Figs. 5 and 6 appear to be associated with the decay heat model that is built into PARET, but that is not present in the simple Matlab model and, in particular, is not included with the power signal used to "measure" neutron power in the UMLRR system. Since there is no easy way to "turn-off" the decay power model in PARET, the comparison seen in Fig. 5 is about the best that can

be expected in all subsequent comparisons to measured data. The most important conclusion from this test, however, is that the point kinetics data used to model the UMLRR (in both the PARET and simple Matlab model) does a great job simulating the system behavior for this zero-power test -- as evidenced in both Figs. 5 and 6.

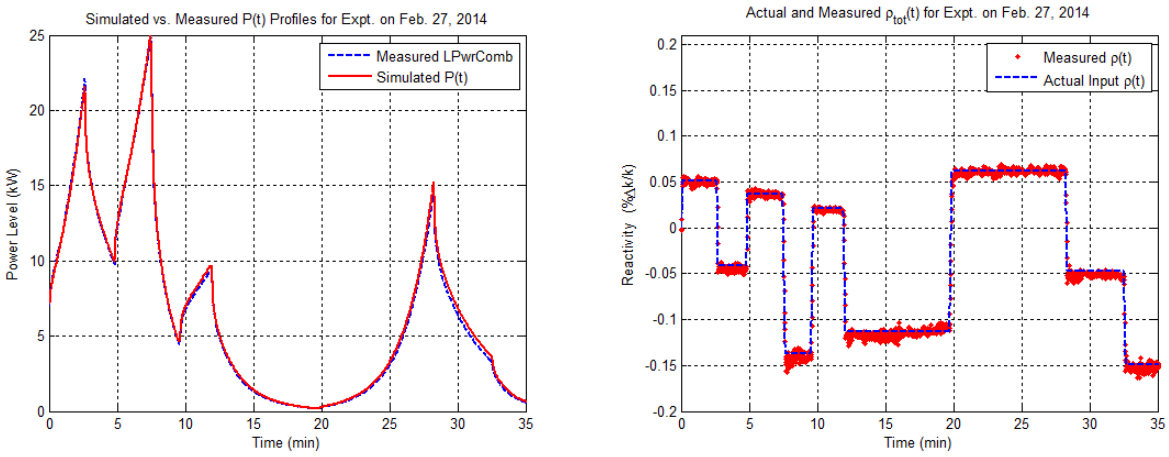


Fig. 6 Summary results for the feedback-free case using a simple Matlab simulation.

With a good reference zero-power model, the next logical step was to validate the feedback coefficients used to model normal operational transients that include heat-up of the fuel and coolant. Within this context, the calculational vs. experimental comparisons for the remaining three reactivity tests are summarized in Fig. 7. In general, the power profiles look good, with reasonable agreement for the two natural convection cases, and acceptable agreement for the Test #1 forced convection case up to about 8 minutes into the transient. For the forced-flow high power case, we have already noted that PARET does not have a xenon reactivity model so that, after 8-10 minutes into the transient, we should expect to see the deviation observed in the top plots in Fig. 7, where the measured data starts to level-off much faster than the PARET simulation -- simply because of the missing xenon component of the total reactivity predicted by the PARET code. Also note that, for the natural convection cases, P(t) never exceeds 50 kW, so xenon effects should be small in these cases, and closer agreement is certainly expected here.

In addition to the power profile comparisons, Fig. 7 also shows the predicted and measured reactivity feedbacks vs. time for the three reactivity transients that include temperature and void feedbacks. This information is important since one of the primary motivations for doing these benchmark studies was to evaluate the adequacy of the reactivity coefficients used in the PARET simulations. Although we cannot measure the individual feedback contributions, the total calculated $\rho(t)$ compares reasonably well with the "measured" values using the measured P(t) results and the inverse kinetics algorithm discussed in Ref. 7. Again, the curves for the high power case clearly start to deviate because of the missing xenon component, and there is indeed a bit of noise in the measured $\rho(t)$ signal, but this is expected because of the noise variation observed in the actual P(t) signal that comes from the two linear power channels (the two linear power channel signals are averaged to give the measured P(t) used in these analyses).

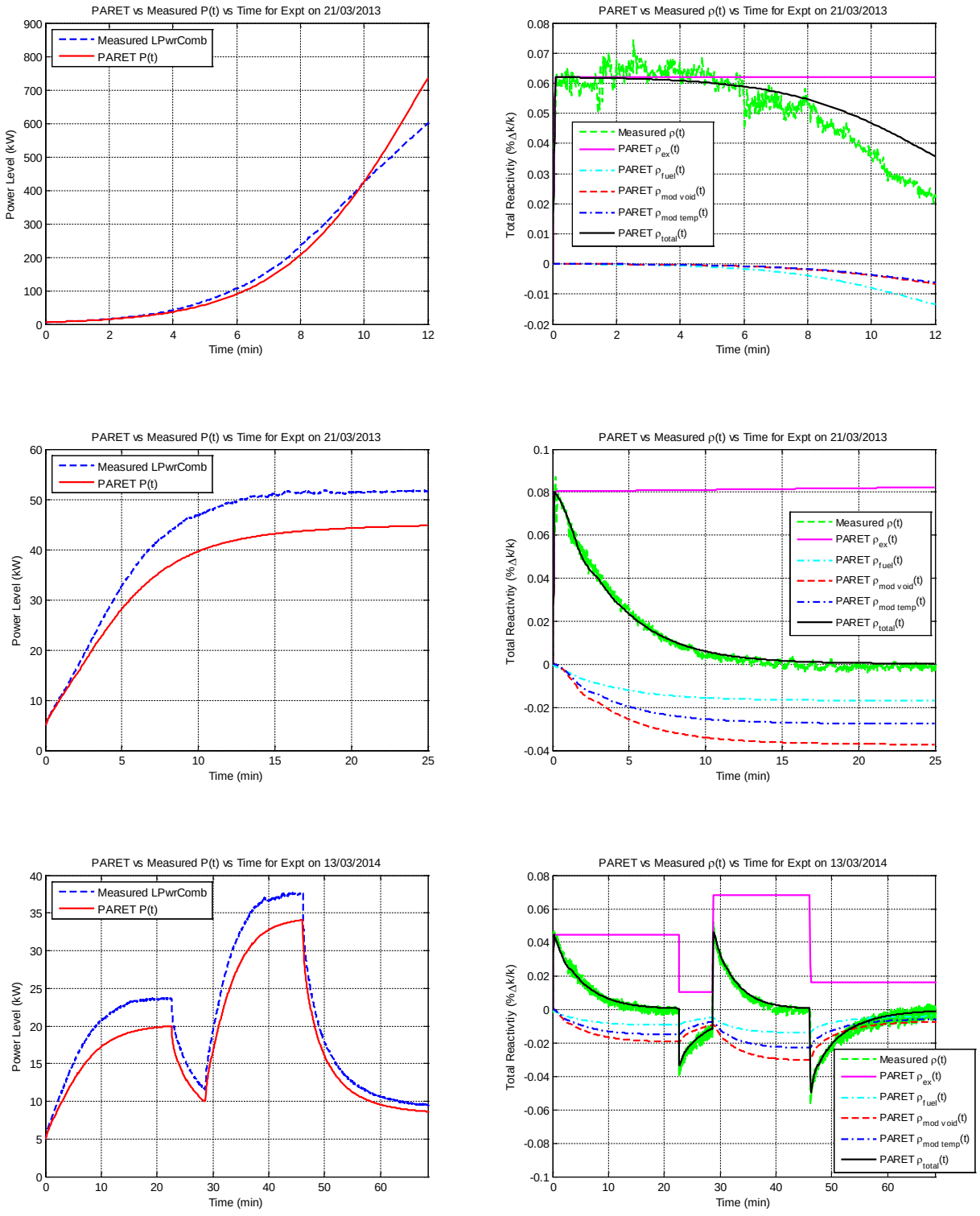


Fig. 7 Summary results for the three reactivity-induced reactor scenarios.

Top Plots: Test #1 forced flow case with single RegBlade withdrawal
Middle Plots: Test #2 natural convection case with single RegBlade withdrawal
Bottom Plots: Test #3 natural convection case with multiple RegBlade movements

Finally we note that all three feedback mechanisms (fuel temperature, moderator temperature, and moderator density) are important in these reactivity-induced test cases. For the forced flow case, the fuel temperature is the dominate component since, with a core flow rate of 1650 gpm, the coolant temperature change remains fairly small. In contrast, for the natural convection cases, the moderator temperature and density contributions are more prominent, since the coolant temperature changes in these cases are usually much larger than for the forced flow case. Thus, everything here behaves qualitatively as expected, and the total compensated feedback seems to be predicted fairly accurately.

Concerning the flow-induced transients, the summary results for these cases are given in Figs. 8 and 9, respectively, for the pump-off and pump-on tests. Focusing first on the pump-off case, one can observe several interesting aspects associated with this loss-of-flow scenario -- most important, of course, is the good agreement of the calculated and measured P(t) profiles. In addition, however, this transient scenario involves a flow reversal from forced down flow (1700 gpm) to natural convection up flow which, in itself, is another great test of the PARET code and the 2-channel UMLRR PARET model used to simulate this dynamic behavior.

In particular, the temperature curves and channel mass flow rate curves show that the core heats up quickly (in about 10 seconds) as the coolant flow rate goes to zero, and then starts to cool down a bit (at about 40 seconds) as natural convection up through the core is established. The mass flux ($kg/s/m^2$) is shown for both the hot and average channels to emphasize that the flow inversion takes place a little sooner in the hot channel than in the average channel, and this is as expected because of the higher temperatures and greater net density difference that exists to support the buoyancy-driven flow in the hot channel. In the actual core, upward flow in the heated channels will occur at different times depending on the local plate power density and behavior of neighboring channels. However, in the PARET 2-channel model (with the "average" channel representing essentially the full core), flow reversal and the development of the natural convection flow occurs simultaneously in all the channels except one (i.e. the single hot channel explicitly modeled in the code).

This disparity between the model and actual reactor can be seen in the reactivity vs. time plot, where the minimum in the calculated $\rho(t)$ curve occurs about 10 seconds later than the actual measured reactivity profile. In addition to the timing, the magnitude of the predicted reactivity change is also less than the measured value because the model forces all the core channels (except one) to be cooled at the same rate, whereas in the actual system, each channel heats up individually to the point where it can support natural convection flow (i.e. buoyancy forces balance the friction forces). Overall, however, the PARET model does a good job of simulating this loss-of-flow test, and it appears that the reactivity coefficients used here give a reasonable representation of the inherent temperature feedbacks associated with this transient.

For the pump-on or cold water insertion test as seen in Fig. 9, the dynamics are very fast since full pump capacity is established in only a few seconds. For this test, the initial positive reactivity that is established by the cold water insertion is well-predicted by the PARET model, which again helps validate the reactivity coefficients used within the PARET model. However, as apparent in Fig. 9, the measured $\rho(t)$ curve shows a small decrease after about 10-12 seconds into the transient that is not reproduced in the model. Because PARET does not reproduce this behavior, the predicted positive $\rho(t)$ is over-estimated by a small amount for much of the transient, which causes the predicted power profile, P(t), to be high relative to the measured data.

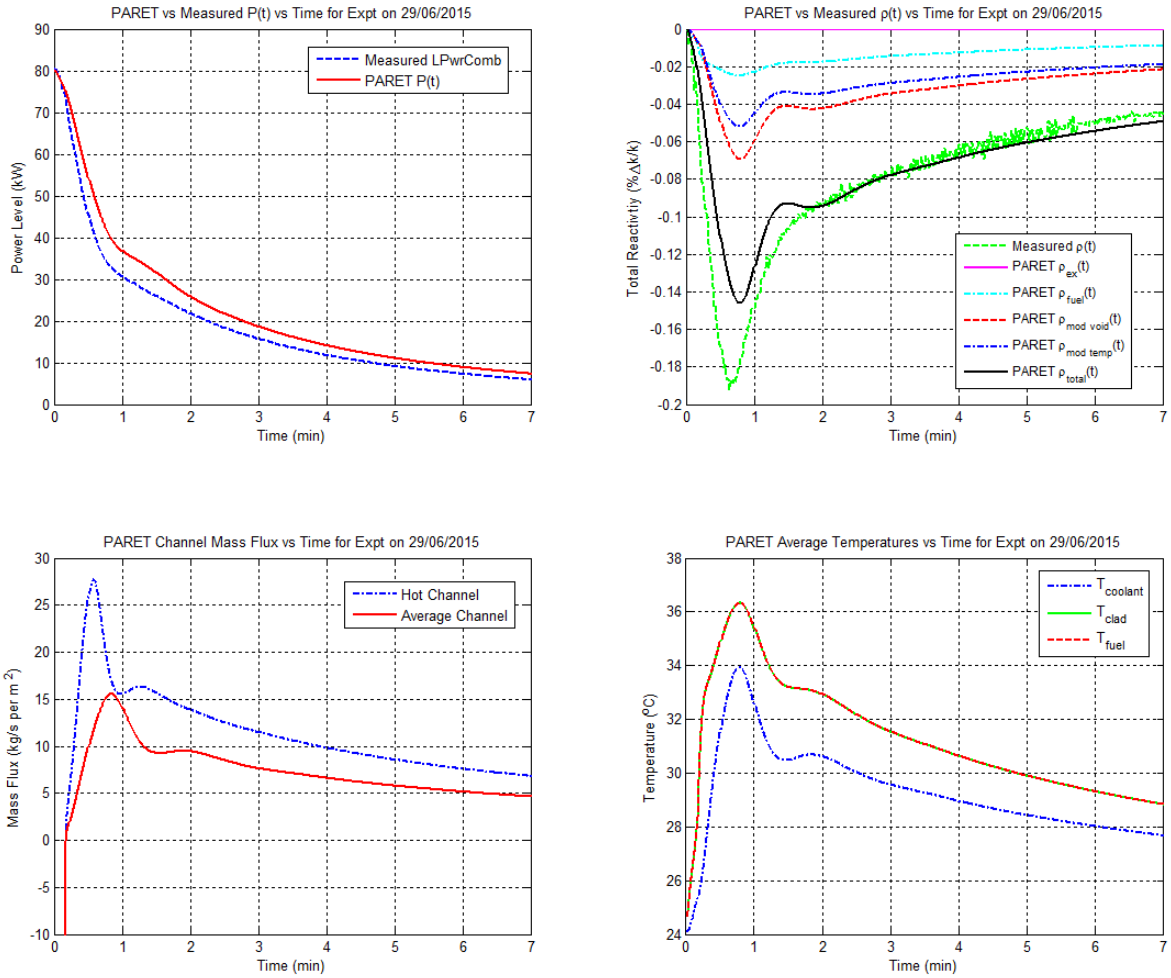


Fig. 8 Summary results for the pump-off test from June 29, 2015 with P_o = 80 kW.

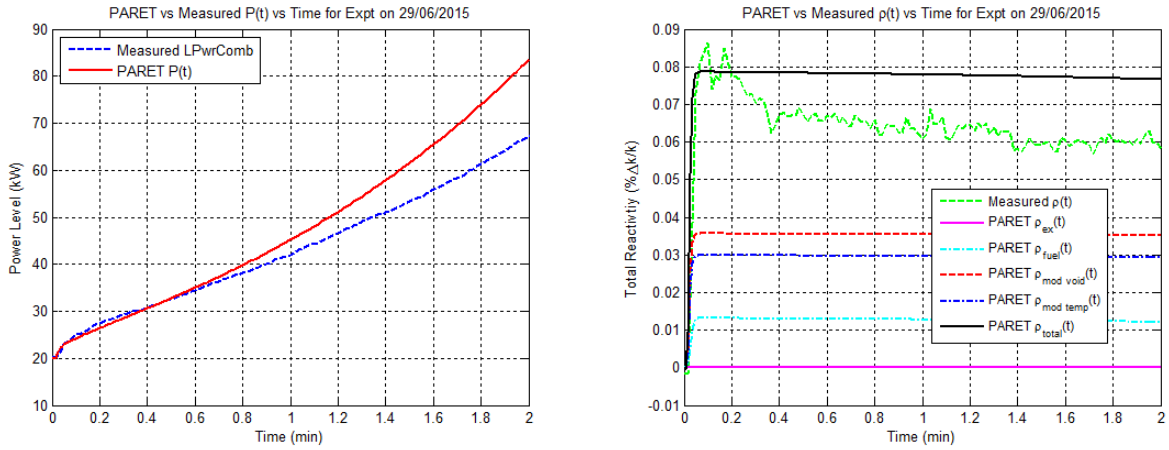


Fig. 9 Summary results for the pump-on test from June 29, 2015 with P_o = 20 kW.

Although it is not clear what causes the initial decrease in reactivity in the real system, it is apparent that this phenomena accounts for the observed differences in the calculated and measured $\rho(t)$ and $P(t)$ profiles for this test. Until the real cause of the observed initial reactivity decrease is understood in more detail, PARET cannot be expected to model this particular transient scenario with any better accuracy than seen here.

Accounting for Uncertainty in the Reactivity Coefficients

The important conclusion from the above tests is that the PARET model with the "new evaluation" reactivity coefficients listed in Table 2 does an overall sufficient job at simulating the actual dynamic behavior of the UMLRR for both the reactivity and flow-induced transients studied here. This summary result is quite positive and, based on our experience with these tests, we intend to use the base PARET model developed here and the data from Table 2 as a reference point for subsequent best-estimate modeling of the UMLRR dynamic performance. However, a nagging discrepancy (that is still unresolved) is that the measured isothermal temperature coefficient is more than a factor of two below that suggested by the calculated reactivity coefficients used in these studies (see Fig. 3). As a demonstration that a major reduction in the reactivity coefficients (coupled with the existing PARET model) is unrealistic, the transient cases discussed above were re-run with a reduction of 25% in all the reactivity coefficients. Selected results from the "nominal' and "reduced" coefficient cases are summarized in Fig. 10, with the "best-estimate" results on the left side and the results with the reduced reactivity coefficients on the right side. As apparent, the best-estimate calculations with the nominal feedback coefficients clearly do a better job at predicting actual reactor behavior, and these comparisons show that a reduction in the reactivity coefficients by just 25% is not really justified -- strongly suggesting that a factor of two reduction is not reasonable! Thus, at this point, the difference in the calculated and measured isothermal temperature coefficient, α_{ITC}, will be left unresolved, and we will move forward using the "new evaluation" reactivity coefficients from Table 2 in future best-estimate studies using the PARET code.

However, for the UMLRR safety analyses, we always want to error on the conservative side of things so that we are assured that reactor safety will not be compromised under any credible circumstances. Thus, for any "worst-case" calculations, higher than normal power and coolant inlet temperatures are usually assumed, and lower than nominal flow rates are used -- all to guarantee that conservative limits are imposed on reactor operations. Thus, within this context, when worst case studies are performed, the nominal reactivity coefficients will be reduced by 25% to account for any uncertainty that may exist in these values -- since, as seen in Fig. 10, this level of reduction leads to a more conservative prediction of $P(t)$ in each of the cases shown here. Note however, if a cold water insertion event is analyzed, then the magnitude of the coefficients would be increased by 25%, since in this case we want to be conservative with the prediction of the amount of positive reactivity addition to the system.

Summary and Conclusions

The goal of this study was to establish a reasonable set of reactivity coefficients for the UMLRR for use within subsequent PARET transient analyses. A re-evaluation of the work done by Michael Pike was completed (see Ref. 2), and a new set of nominal reactivity coefficients (α_{Tf}, α_{Tm}, and α_V for the fuel temperature, moderator temperature, and moderator void, respectively) were determined. These new feedback coefficients were used in a series of validation tests that compared PARET simulations with measured data from a set of relatively mild reactivity

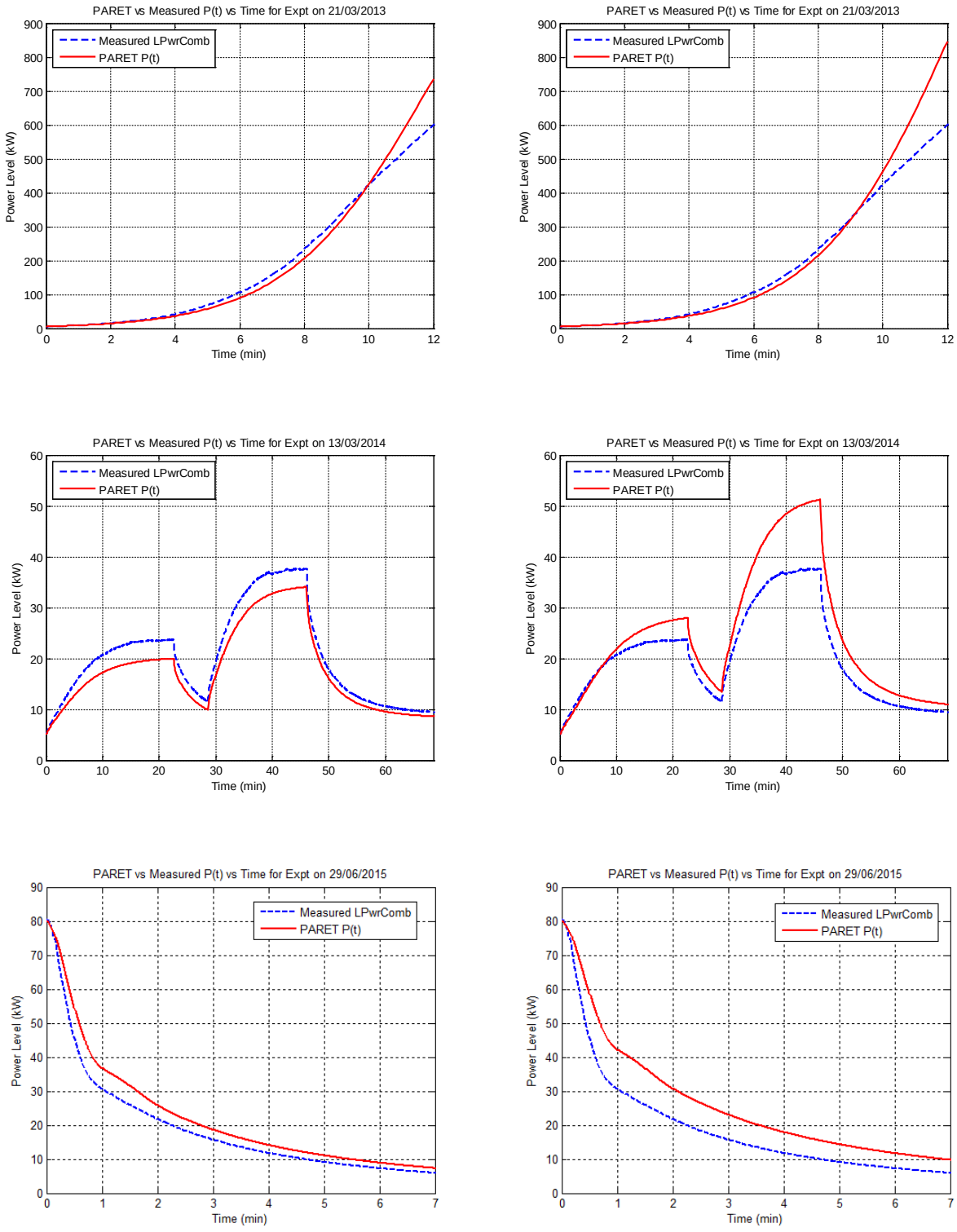


Fig. 10 Selected results with nominal (on left) and reduced (on right) reactivity coefficients.

Top Plots: Reactivity Test #1 forced flow case
Middle Plots: Reactivity Test #3 natural convection case
Bottom Plots: Pump-Off Test with P_o = 80 kW

and flow-induced transients within the UMLRR. In general, the results were quite favorable, showing that the PARET models and new reactivity coefficients could predict actual reactor performance with reasonable accuracy. Thus, it appears that the base kinetics data and the new reactivity coefficients can be utilized with reasonable confidence in subsequent PARET dynamic analyses of the UMLRR. In addition, for "worst-case" studies, it was decided to modify the coefficients in a conservative manner by 25% to account for any uncertainties that may exist in both the models and their input reactivity coefficient information.

The difference in the calculated and measured isothermal temperature coefficient that was originally identified in Ref. 2 is still unresolved. However, based on the positive results of the validation tests completed as part of this work, we have decided to set aside this unresolved issue for now, and move forward using the new reactivity coefficients listed in Table 2 in all future best-estimate studies using the PARET code. Resolving the issue with the computed and measured isothermal temperature coefficient will have to wait for another day…

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8. J. R. White, "Measuring Integral Blade Worths Curves within the UMLRR," one of a series of labs for the Reactor Experiments course at UMass-Lowell. The Inverse Kinetics method is discussed, validated, and then used to generate a blade worth curve as part of this lab. The "validation" component is where the "zero-power" transient test fits within this overall lab exercise.