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CALCULATION OF SAVANNAH RIVER K REACTOR MARK-22 ASSEMBLY LOCA/ECS POWER LIMITS

by

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ABSTRACT

This paper summarizes the results of TRAC-PF1/MOD3 calculations of Mark-22 fuel assembly loss-of-coolant accident/emergency cooling system (LOCA/ECS) power limits for the Savannah River Site (SRS) K Reactor. This effort was part of a larger effort undertaken by the Los Alamos National Laboratory for the US Department of Energy to perform confirmatory power limits calculations for the SRS K Reactor.

A method using a detailed three-dimensional (3D) TRAC model of the Mark-22 fuel assembly was developed to compute LOCA/ECS power limits. Assembly power was limited to ensure that no point on the fuel assembly walls would exceed the local saturation temperature.

The detailed TRAC model for the Mark-22 assembly consisted of three concentric 3D vessel components which simulated the two targets, two fuel tubes, and three main flow channels of the fuel assembly. The model included 100% eccentricity between the assembly annuli and a 20% power tilt. Eccentricity in the radial alignment of the assembly annuli arises because axial spacer ribs that run the length of the fuel and targets are used. Power tilt arises from assumptions regarding control and safety rod positions.

Wall-shear, interfacial-shear, and wall heat-transfer correlations were developed and implemented in TRAC-PF1/MOD3 specifically for modeling flow and heat transfer in the narrow ribbed annuli encountered in the Mark-22 fuel assembly design. We established the validity of these new constitutive models using separate-effects benchmarks. Preliminary assembly benchmark calculations made for the SRL SPRIHTE and FA Rig experiments indicate that TRAC with appropriate modeling can predict assembly power limits accurately.

TRAC system calculations of K Reactor indicated that the limiting ECS-phase accident is a double-ended guillotine break in a process water line at the pump discharge (i.e., a PDLOCA). The fuel assembly with the minimum cooling potential is identified from this system calculation. Detailed assembly calculations then were performed using appropriate boundary conditions obtained from this limiting

system LOCA. Coolant flow rates and pressure boundary conditions were obtained from this system calculation and applied to the detailed assembly model.

The detailed TRAC assembly model incorporated best-estimate geometric and assembly power information as well as limiting or bounding assumptions. Power limits calculated using this TRAC methodology are influenced by a variety of items, including constitutive models, numerical methods, modeling assumptions, model input, boundary conditions, geometric input, power-related input parameters, etc. To account for modeling uncertainties, a comprehensive uncertainty analysis including detailed sensitivity studies was performed to establish a conservative assembly power limit.

I. INTRODUCTION

The performance of the Savannah River K Reactor emergency cooling system (ECS) during loss-of-coolant accidents (LOCA) was analyzed with the TRAC-PF1/MOD3 computer code[1] to determine power limits for safe operation with Mark-22 fuel. This effort was part of a larger effort undertaken by the Los Alamos National Laboratory for the US Department of Energy to perform confirmatory power limits calculations for the Savannah River K Reactor.[2]

A power limits methodology based on the $T_{wall} = T_{sat}$ criterion adopted by the Savannah River Laboratory (SRL)[3] was developed to compute assembly power limits. This methodology limits the reactor power such that no point on the fuel annuli walls will reach the local liquid saturation temperature during an accident.

A TRAC LOCA system calculation was used to provide boundary conditions for a detailed Mark-22 single assembly model. TRAC system calculations of the K Reactor indicate that the limiting ECS-phase accident is a double-ended guillotine break (DEGB) in a process water line at the pump discharge.[4] The fuel assembly with the minimum cooling potential was identified from this system calculation, and coolant flow rates and pressure boundary conditions also were obtained and applied to the detailed assembly model.

The thermal-hydraulic performance of the Mark-22 assembly under LOCA/ECS conditions was investigated with two different TRAC models. In the first, the flow channels were modeled as concentric one-dimensional flow annuli (1-theta). This simple model was used to perform flow and pressure drop sensitivity studies because a relatively large number of runs could be constructed easily and executed efficiently.

A second detailed (4-theta) TRAC assembly model, which was used to compute the base-case power limit, included the capability to simulate multidimensional effects resulting from power tilt and fuel element eccentricity within each annular channel. The flow channels are modeled by three TRAC VESSEL components, each with 4 azimuthal and 16 axial nodes. The fuel and target elements were described by 32 HEAT STRUCTURE components.

Wall-shear and interfacial-shear packages were developed and implemented in TRAC PF1/MOD3 specifically for modeling flow in the narrow ribbed annuli encountered in the Mark-22 fuel assembly design.[5] These shear packages were validated with the Savannah River experiments of Steimke,[6] Whatley,[7] Steimke,[8] Whatley,[9] and Johnston.[10] Code benchmark calculations also were performed using the Babcock and Wilcox (B&W) Annular Flow Distribution (AFD) experiments.[11] Code calculations were in good agreement with pressure gradient and void fraction data. Trends in air entrainment vs liquid flow were modeled well, but some discrepancies were observed in the magnitude of the code-calculated air entrainment. This result was not entirely unexpected because there were discrepancies in air entrainment rates between the data sets. The SRL Rig B heat-transfer experiments performed by Guerrero[12] were analyzed, and a heat-transfer correlation suitable for air/water, nonboiling, forced-convection downflow in narrow ribbed annuli was developed and implemented in TRAC. Preliminary assembly benchmark calculations have been made with TRAC for the SRL SPRIHTE[13] and FA Rig[8] experiments. Initial results indicate that TRAC with appropriate modeling can predict the $T_{wall} = T_{sat}$ power limits obtained in these experimental facilities accurately.

An uncertainty analysis was performed to conservatively bound the nominal power limit calculation to account for code modeling and geometric uncertainties. Where possible, the uncertainty factors have been estimated from TRAC sensitivity calculations.

Section II discusses the Los Alamos TRAC-based power limits methodology. Section III summarizes the detailed TRAC assembly model. Section IV reviews the results of the nominal power limit calculation. Sections V and VI discuss the results of the sensitivity and uncertainty analyses, respectively. Section VII summarizes the results of the Mark-22 power limit calculation.

II. POWER LIMITS METHODOLOGY

The Los Alamos Mark-22 assembly power limit methodology is based on the $T_{wall}=T_{sat}$ criteria developed by SRL.[3,14] Unfortunately the TRAC system calculation does not provide the detailed assembly thermal-hydraulic information required to establish power limits under LOCA/ECS conditions. Therefore, detailed assembly calculations had to be performed using appropriate boundary conditions obtained from the limiting system LOCA to determine power limits. That is, for a TRAC LOCA system calculation, the 18 lumped fuel assemblies used to represent the core had to be searched to identify the limiting, hottest, or "worst-case" assembly. The assembly exhibiting the largest power-to-heat-removal-capability ratio is considered to be limiting. At steady conditions, the limiting assembly easily can be identified by comparing ratios of

$$\mathrm{Power}/\dot{m}C_p(T_{sat} - T_{inlet}) ,$$

where

Power = assembly power,
m = assembly liquid mass flow,
Cp = liquid specific heat capacity,
T_{sat} = assembly outlet saturation temperature, and
T_{inlet} = assembly inlet liquid temperature.

Boundary conditions for the detailed assembly were obtained from the most limiting assembly in the most limiting LOCA. TRAC system calculations performed by Motley and Morgan[4] indicated that the pump-discharge LOCA (PDLOCA) was most limiting for the ECS phase. To identify the limiting time and assembly in the TRAC PDLOCA system calculation, plots of Power/mCp($T_{sat} - T_{inlet}$) vs time (i.e., the ratio of instantaneous power to the cooling potential of the process water vs time) were generated for each of the 18 lumped assemblies used in the TRAC system model. The assembly exhibiting the maximum power- to-flow ratio was considered to be most limiting. Figure 1 shows such a plot for the most limiting assembly in the TRAC PDLOCA calculation. The time when the peak occurs establishes the boundary conditions for the TRAC assembly model, i.e., decay power, assembly pressure drop, and total assembly liquid flow. Appropriate variables were extracted from the TRAC system calculation at this critical point and were used as boundary conditions for the TRAC detailed assembly model. For our limits calculations, these worst-case conditions were assumed to continue indefinitely; i.e., we assume these conditions were "quasi-steady." That is, the detailed assembly model was run with these boundary conditions while the power was increased with a TRAC controller until a steady state was reached such that the wall temperature equaled the local saturation temperature on one location on the fuel or target annuli.

The validity of the "quasi-steady" assumption was investigated by running the detailed assembly model with transient boundary conditions obtained from the system calculation. For the limiting PDLOCA, the transient and quasi-steady approaches yielded nearly identical power limit results. This was not surprising because at 100+ s into the LOCA, the assembly liquid conditions are changing only slightly and over the period of interest can be approximated by quasi-steady values.

For the limits calculations discussed in this paper, a "nominal" TRAC model was developed that incorporated best estimate geometric and assembly power information as well as limiting or bounding assumptions. These assumptions are summarized below.

1. Deposited power, axial shape, exposure, and tube power fractions were obtained from an earlier TRAC analysis of flow instability power limits.[15] These input data were based on information obtained from SRL.

2. Channel flow splits into the individual annuli of the Mark-22 assembly were computed using the B&W AFD correlation.[11] Inputs to the B&W correlation include assembly pressure drop and total assembly liquid flow. This information was obtained from the TRAC system calculation at the identified limiting point.

3. The limiting assembly identified from the system calculation provided the inlet and outlet pressures for the assembly model.

4. The SRL estimate of maximum assembly inlet temperature of 35.5°C, which was determined to bound RELAP5 system calculations,[14] also was used in the TRAC assembly calculation. This was a conservative bound of the inlet liquid temperature of 33.6°C at the limiting point as determined from the TRAC system calculation.

5. The saturation temperature was reduced by 5°C to account for possible hot spots resulting from localized voiding in subchannels consistent with assumptions made by SRL for their FLOWTRAN analyses.[3,14,16] This conservatism apparently resulted from azimuthal temperature variations observed in the Rig FB experiments performed by Johnston.[10]

6. Inner and outer purge channels were not modeled, and thus, the outer wall of the outer target and inner wall of the inner target target were assumed to be adiabatic. This assumption is conservative because, at most, approximately 5% of the total assembly flc · is expected to flow in the purge channels at the system calculation conditions.[11] Thus, in reality, there would be some cooling of the inner and outer target elements that our model does not take into account.

7. Complete eccentricity (100%) was modeled by postulating radial movement of the inner fuel and target annuli such that the ribs contacted adjacent walls. Eccentricity in the radial alignment of the assembly annuli arises because of the use of axial spacer ribs that run the length of the fuel and targets. This eccentricity is depicted graphically in Fig. 2, which shows a cross section of an eccentric assembly. Eccentricity is modeled hydraulically by adjusting subchannel axial and azimuthal flow areas accordingly. Azimuthal flow areas in the smallest subchannels were left partially open to simulate possible bowing and nonuniform axial eccentricity. This was found to be conservative because liquid tends to be pushed out of these hottest subchannels as temperatures increase. Rod bow was not modeled specifically.

8. A 20% power tilt was assumed to exist such that the highest power occurred in the subchannel with the smallest flow area. Power tilt arises because of assumptions regarding control and safety rod positions. SRL showed this assumption to be conservative.

9. The ribs on the inner target are assumed to be oriented the same as the ribs between the outer target and outer fuel. Sensitivity studies have not been performed to assess the importance of this assumption. An azimuthal-rib flow-void donor model similar to that developed for FLOWTRAN was not used in the TRAC model.

III. MODEL DEVELOPMENT

A cross section of the detailed TRAC assembly model is shown in Fig. 2. The model consisted of three separate annular flow channels that simulated the annuli between the inner target and inner fuel annulus, between the inner and outer fuel, and between the outer fuel and outer target.

Each of the three annular flow channels was modeled using a TRAC VESSEL component as shown in Fig. 3. Water was injected at the upper boundary of the annulus into each quadrant through FILL components. The actual liquid flow rates were calculated from the B&W AFD Test data correlations. The pressure drop across the assembly and total assembly flow rate that were used as input to the B&W correlation were obtained from the TRAC system calculation. The temperature of the injected liquid was set to 35.5°C rather than using the value of 33.6°C from the TRAC system calculation. The B&W correlation yielded a total flow for each annulus, which was allocated among the quadrants according to flow areas. The water injection was equal in each quadrant for the concentric rod case. For the eccentric rod or nominal case, the flow into each quadrant was assumed to be directly proportional to the quadrant flow area. This assumption regarding subchannel or quadrant flow splits had only a minor effect on power limits because azimuthal flow between the quadrants was allowed and azimuthal redistribution of liquid was calculated to take place axially down the assembly.

At the top of the annulus, pressure boundaries were modeled with BREAK components. These components allowed air to be entrained down the annulus at rates consistent with the TRAC constitutive packages. Liquid was not allowed to flow into or out of these boundary cells.

BREAK components (connected to the bottom of the annulus) were used to establish pressure boundary conditions consistent with the TRAC system calculation. Air and water were allowed to flow into and out of these boundary cells. PIPE components, which connected the BREAK and FILL components to the VESSEL component, were oriented horizontally and were short to minimize pressure drops. Thus, the pressures set in the BREAK components established the pressure boundary conditions for the annulus.

Figure 4 shows the noding of a typical VESSEL component. There were 2 radial, 4 azimuthal (4-theta), and 16 axial nodes. Only the outer radial ring had flow. The axial nodes were 0.257 m long except for the bottom and top cells, which were 0.232 and 0.108 m, respectively.

Figure 2 also indicates how the hydrodynamic conditions in the VESSEL components were coupled to each other through HEAT STRUCTURE components, which represented the fuel and target elements of the Mark-22 assembly. There were 32 HEAT STRUCTURE components, one every 45° for each of the fuel and target elements. Each individual heat structure was composed of 7 radial and 15 axial nodes consistent with the adjacent hydraulic cell noding. The 900 numbers in the figure represent these components. A HEAT STRUCTURE was required for every 45° because of the orientation of the ribs. For example, it can be seen that quadrant 1 of the inner and outer annuli overlaps quadrant 1 of the middle annulus over a 45° interval, and the remaining 45° of quadrant 1 of the inner and outer annuli overlaps quadrant 4 of the middle annulus.

The azimuthal power tilt and the rod eccentricity were applied in the same direction (i.e., toward the top of Fig. 2). The rib between quadrants 1 and 4 of the middle annulus was assumed to completely seal off azimuthal flow between the quadrants. However, in the inner and outer annuli, the rib between quadrants 1 and 2 and the rib between quadrants 1 and 4 were allowed to have nominal azimuthal flow. Because liquid was injected directly into each quadrant at a flow rate given by the B&W correlation, it was conservative to allow azimuthal flow out of these hotter quadrants.

Temperature-dependent values of density, heat capacity, and thermal conductivity for the target and fuel elements were used in the analysis. Deposited power, power fraction, and axial and radial power shapes were obtained from previous TRAC calculations of Mark-22 assembly power limits performed by Rodriguez.[15] The outer wall of the outer target and inner wall of the inner target were assumed to be adiabatic. This assumption was conservative because the B&W correlation shows that at most approximately 5% of the total assembly liquid flow is through the inner and outer purges at the boundary conditions used.

Liquid injection through the FILL components was ramped up to final values in 10 s. Power was adjusted using a TRAC PID (proportional, integral, differential) controller to achieve steady-state conditions such that the surface temperature equaled the saturation temperature of the liquid at some point on the wall.

IV. NOMINAL MARK-22 ECS POWER LIMIT

Boundary conditions defining the nominal or limiting case were obtained from a TRAC system calculation of the PDLOCA.[4] The quasi-steady-state boundary conditions were determined as described in Sec. II. Other features of the nominal TRAC Mark-22 assembly calculation were discussed in Sec. III.

For power limit calculations, steady-state conditions were achieved by executing the TRAC calculation for a sufficient period of time to permit equilibration of the thermal and hydraulic processes within the model. The assembly power was modulated by a TRAC PID control element until the surface node of any heat structure component used to represent the target or fuel elements reached a maximum temperature of $T_{max} = T_{wall} - 5$. The approach to equilibrium may be observed in the temporal behavior of the assembly power as shown in Fig. 5. For this nominal case, the power approaches steady state in the neighborhood of 40 s from the beginning of the computation.

One of the conditions defining an acceptable modeling result is convergence of the solution with respect to the problem time-step size. That is, the results of the calculation should not be affected by a reduction in the maximum time-step size.

The effect of maximum time-step size on the nominal case was investigated by reducing the maximum time-step size successively from 0.10 to 0.05 to 0.02 s. Results showed that convergence is improved by time-step reduction within the range investigated, but the calculated power limit is not affected by reductions below 0.05 s. Further reduction of the maximum time-step size will not affect the power limit result.

The geometric representation of the assembly model contains features that provide a basis for examination of the thermal and hydraulic characteristics as computed by TRAC. Eccentricity in the flow channels and power tilt in the fuel creates a potential for azimuthal flow between channel sectors. Further, there is a line of symmetry in each annulus such that the flow distribution in half the channel should match the other (symmetric) half.

Calculational results at steady-state conditions when the power limit ($T_{wall} = T_{sat} - 5$) is reached show the thermal-hydraulic state of the assembly predicted by the TRAC model. The annulus axial liquid flow distribution as a function of elevation indicates that the axial liquid flow rates vary axially consistent with the azimuthal flow redistribution. Because of the annular nature of the flow computed for the middle channel, there is little azimuthal liquid flow except near the channel bottom where the liquid pools. There is liquid flow away from the hot or limiting sector near the bottom of the outer channel.

Symmetry between flow sectors is exhibited in all channels. Void fraction profiles suggest that the inner channel is in bubbly to churn-transition flow, whereas most of middle channel is in annular flow. In the middle channel, liquid pools over the last 0.5 m near the bottom of the channel. The outer channel appears to be in transition between churn and annular flow.

The calculated void profiles for all three channels are consistent with the calculated pressure distributions.

Air entrainment is calculated (by TRAC) consistent with the constitutive models to match the PDLOCA boundary conditions. The middle channel, consistent with single-annuli separate-effects tests for annular flow, shows essentially no air entrainment. The inner and outer channels show an increase in vapor flow down the assembly consistent with heating effects.

V. SENSITIVITY STUDIES

A number of sensitivity calculations were performed to assist model development efforts, to better understand the TRAC assembly calculations, and to provide input into our uncertainty analyses. This section summarizes some of those calculations. The results of the sensitivity studies discussed in this section are included in Table I.

A. Assembly Channel Flow Split

The sensitivity of TRAC-calculated power limits to variations in the inlet-channel flow distribution was analyzed using the simplified "1-theta" assembly model. The magnitude of flow variation was based on the accuracy of the B&W flow split correlation,[11] which is typically in the neighborhood of 2.5% at the 1σ

TABLE I

SUMMARY OF SRL AND LOS ALAMOS SENSITIVITY RESULTS

Sensitivity Variable	% Reduction in Power (SRL)	% Reduction in Power (Los Alamos)
Inlet Liquid Flow	26.4	26.4 (SRL)
Tube Power Fraction	2.2	2.2 (SRL)
Heat-Transfer Coefficient	31	16.4
Wall Friction	0	0 (SRL)
Channel Inlet Void	14.2	N/A
Channel Inlet Flow Split	N/A	3.5
Rib Cross Flow Model	16.8	16.8 (SRL)
BEF Form Loss	0	N/A
Interfacial Drag	0	0 (SRL)
Tube Thickness	5.9	5.9 (SRL)
Axial Heated Length	2.4	2.4 (SRL)
Assembly Pressure Drop	Bounded (-0.5 psig)	14.8
Power Tilt	Bounded (20%)	Bounded (20%)
Eccentricity	Bounded (100%)	Bounded (100%)
Wall Peaking/Local Voiding	Bounded (T_{sat}–5 K)	Bounded (T_{sat}–5 K)
Inlet Flow Oscillations	0	0 (SRL)
Axial Power Shape	Bounded (1.55 axial peaking)	Bounded (1.55 axial peaking)
Decay Heat Curve	Bounded (ANS)	Bounded (ANS)
Inlet Liquid Temperature	Bounded (T_{inlet} vs time)	Bounded (T_{inlet} = 35.5°C)
Rod Bow	Bounded (2.0 in.)	Not currently modeled

level over its range of applicability. Thus, a 2σ variation in flow distribution is interpreted as a 5% redistribution of flow between the channels or annuli for a given value of total assembly inlet flow. An assembly flow of 15 gal./min was selected for the nominal flow condition for this study. This flow was the lowest value used with the 1-theta model and was determined to be the most sensitive to flow variations.

By trying different ways to redistribute the flow among the assembly channels or annuli, it was found that removing flow from the channel adjacent to the surface with the maximum fuel temperature and transferring it into the channel not adjacent to that fuel element produced the largest effect on the computed power limit. Performing this operation on the 15-gal./min assembly flow case yielded a 3.5% reduction in power limit for a 5% (2σ) variation in flow distribution. This is the basis for the Channel Flow Split sensitivity value presented in Table I.

B. Assembly Pressure Drop

Variation in assembly pressure drop produces change in the inlet channel flow distribution as expressed in the B&W flow split correlation because the flow splits are correlated with pressure drop as well as flow rate. This split affects the power limit considerably less than do variations in the total flow because of the two-sided cooling of the fuel elements. Thus, redistribution of flow from one flow channel to the adjacent channel does not change the fuel temperature significantly. This effect was evaluated by performing several calculations with the 1-theta assembly model at constant inlet flow and different values of pressure drop. The maximum 2σ sensitivity of power limit to variations in pressure drop at a fixed inlet liquid flow rate was found to be 14.8% at 15 gal./min. For these calculations, the 1-theta model was used to compute power limits with assembly flows and pressure drops ranging from 15 to 70 gal./min and -4.17 to 1.35 psid, consistent with the B&W parametric test data. For these sensitivity runs, the B&W test data provided the boundary conditions for the TRAC calculation.

C. Heat-Transfer Coefficient

The validity of wall-temperature predictions made for power limit analysis depends to a large extent on the accuracy of the wall heat-transfer coefficient. Thus, it is important to use a correlation that correctly reflects the geometry and the behavior of the fluid in the flow path under consideration.

The hydraulic conditions of the Mark-22 fuel element, i.e., co-current down flow of an air-water mixture in a narrow ribbed annulus, are sufficiently unusual and unique that correlations developed for other applications may not be appropriate. Accordingly, SRL and Idaho National Laboratory (INEL) have performed prototypical experiments intended to provide applicable heat-transfer information relative to Mark-22 assembly power limits.[12,17]

Experimental results from the Rig FA[8] and Rig B[12] experiments are available from SRL. These two test rigs were quite similar but each used a different approach toward construction of the heater tubes. Rig FA had an aluminum tube coated with flame-sprayed insulating material surrounded with an electrically conducting layer to provide heat generation. In Rig B, the entire stainless-steel tube wall generated ohmic heating. In both cases, the heat-transfer surface temperature had to be inferred by performing a conduction calculation using a temperature measurement on the opposite (outside) surface (i.e., an inverse conduction problem).

Heat-transfer coefficients obtained from Rig B would be expected to be more reliable because of a large uncertainty in the uniformity of thickness and material porosity of the flame spray layers on the Rig FA test section. Furthermore SRL used the Rig B data as the basis for the correlation used in FLOWTRAN.[16] Thus, only the Rig B data were used in the development of a heat-transfer correlation suitable for the Mark-22 assembly.

The INEL experiments[17] are also highly prototypical of the Mark-22 assembly geometry. They differ from the SRL experiments in that the test section hydraulic diameter is larger and the inner wall is the heat-transfer surface. The combination of these rigs (Rig B and INEL) provided a basis for including the effect of hydraulic diameter in the heat-transfer correlation.

Validation of the test facility and experimental method was provided by a series of single-phase tests performed with the INEL test facility. Among these data are a series of 40 single-phase runs made over a substantial range of boundary conditions. The single-phase heat-transfer behavior exhibited by this test section are shown in Fig. 6. These data are in excellent agreement with the Dittus-Boelter correlation,[19] thus supporting the validity of the results of these experiments.

The experimental data from both Rig B and INEL were organized so that they could be subjected to regression computations to determine the influence of the test variables. The usual way to correlate turbulent (high-flow-rate) heat-transfer data is to use the Nusselt equation,

$$Nu = a Re^{b} Pr^{c} , \qquad (1)$$

and fit the coefficients to the data with a regression analysis. When two-phase flow is involved, attempts often are made to incorporate the two-phase fluid state into the dimensionless variables. This is the approach SRL followed in developing their correlation for use in FLOWTRAN. The Reynolds number was computed using the liquid-phase velocity. The utility of the Nusselt equation in single-phase flow is the generality provided by the similitude relationships in applying the correlation to other geometries and other fluids. However, in two-phase flow, the applicability of these dimensionless parameters is uncertain, and correlations of this kind are not guaranteed. In fact, the correlation obtained by SRL is not a very good fit of the data shown in Fig. 7. The upper and lower 95% confidence limits are 56% and 36%, respectively, with an r^2 of 0.47. One of the difficulties in this procedure is the accuracy of the estimate of void fraction over the entire range of the data conditions.

As the experiments were exactly prototypical of the Mark-22 fuel assembly, developing a correlation using dimensionless groups is unnecessary unless doing so captures an important effect exhibited by the data. The available Rig B data were examined from this viewpoint to determine if a better correlation could be obtained using dimensionless groups. To do this, we processed the data using the Number Cruncher Statistical System (NCSS)[18] on a personal computer.

Using methods available in NCSS, the heat-transfer coefficient was tested for sensitivity to the test boundary conditions, including water and airflow rate, void fraction, and fluid temperature. The only sensitivities detected were to liquid flow and fluid temperature. After some experimentation, we determined that a suitable correlation could be obtained by fitting the heat-transfer coefficient to the superficial Reynolds number. The resulting correlation,

$$h(D_h/D_{ref}) = 2.637Re^{0.664} \ , \qquad (2)$$

captures the influences of both flow and fluid temperature. Attempts to incorporate the Prandtl number and the complete expression for the Nusselt number only degraded the quality of the regression fit. Because nondimensionality is not necessarily required for the way that these data are being used, we concluded that Eq. (2) represents the best available expression for the heat-transfer coefficient.

At low flow rates (4 gal./min and below), the ECS flow within the assembly is calculated to be in the annular flow regime. The heat-transfer coefficient for an annular falling film can be estimated by computing the heat conductance of the film, i.e., k/x. The film thickness can be determined by integrating the velocity profile for a falling film as, for example, usually is presented in the development of film condensation. Kreith[19] gives this solution as

$$\Gamma = \rho^2 g\, x^3/(3\mu) \ ,$$

which, can be solved for x to provide a heat-transfer coefficient as indicated above. This relationship was applied to the SRL Rig B geometry in the low-flow region and compared with experimental heat-transfer coefficients measured in the same flow range.

At the low end of the flow range, the theory merges very well with the experiment. At the higher end, the experimental values rise into values of the high-flow regime(s). This behavior is entirely reasonable. It is expected that the annular flow regime should merge in a smooth manner with the churn-bubbly regimes at higher liquid flow. There is probably a transition through slug flow occurring in this process. The important result here is the excellent agreement with the simple annular flow model at the lowest flow rates.

The correlations and the experimental data are presented in Fig. 8. As indicated, the correlations provide a reasonable representation of the heat-transfer performance of the experiments, and, by inference, the Mark-22 assembly.

We evaluated the sensitivity of the Mark-22 assembly power limit to variation of the heat-transfer coefficient by performing sensitivity calculations with the 1-theta assembly model. The lower bound 2σ confidence interval of the heat-transfer correlation was found by a statistical analysis of the regression fit compared with its base of experimental data. We ran a series of TRAC calculations varying assembly inlet flow over the same range as the base-case calculations (i.e., 15–70 gal./min). The average difference in TRAC-calculated power limits between the nominal and lower bound heat-transfer coefficient curves was 16.4%. This value reflects the per cent reduction in power resulting from heat-transfer sensitivity and is presented in Table I.

D. Total Assembly Flow

The effect of total assembly liquid flow on TRAC-calculated $T_{wall} = T_{sat}$ power limits was investigated using the detailed 4-theta model. Power limits were calculated over an assembly liquid flow range of 5 to 50 gal./min. The assembly pressure drop was fixed at the limiting PDLOCA point, whereas channel flow splits were recomputed based on the total assembly flow. Figure 9 compares the results of TRAC-computed nominal and conservative estimate power limits with FLOWTRAN conservative estimate limits.

The uncertainty in the TRAC-computed assembly flow at limiting PDLOCA conditions was estimated to be 7.5 gal./min.[3] Reducing the assembly liquid flow by this amount in the detailed assembly model resulted in a 26.4% decrease in power as shown in Table I.

VI. UNCERTAINTY ANALYSIS

Power limits calculated using the TRAC-based methodology discussed in Sec. II are influenced by a variety of items, such as constitutive models, numerical methods, modeling assumptions, model input, boundary conditions, geometric input, power-related input parameters, etc.

With the exceptions and limitations noted in Sec. II, most model input parameters are either nominal or best-estimate values. Similarly, influential code constitutive models (i.e., wall and interfacial shear and wall heat transfer) reflect best-estimate correlations developed from appropriate data bases. However, uncertainties in these correlations and model input parameters have to be factored into the power limit uncertainty analyses.

For our uncertainty analysis, individual parameter uncertainties are combined statistically using a simple linear propagation (star-pattern) or root mean square technique similar to that used by SRL with FLOWTRAN.[3] Use of this approach implies that the random variables are independent and the response surface is linear.

Because of time limitations, we were unable to perform extensive sensitivity analyses using our detailed assembly model. As discussed in Sec. V, we performed sensitivity analyses on the more influential parameters such as assembly channel flow split, assembly pressure drop, heat-transfer coefficient, and total assembly liquid flow. These studies were performed using either the 1-theta or 4-theta model. The influence of each parameter change over a range of conditions considered typical of a LOCA (i.e., pressure drop and assembly flow) was investigated. Over this range of conditions a 2σ effect on the computed power limit was determined.

Table I summarizes the results of our parameter uncertainties and compares the TRAC uncertainties with those used by SRL with FLOWTRAN.

For those influential parameters where we had performed sensitivity studies, we used those results to obtain a 2σ effect for each linear parameter variation. The parameters treated in this manner included assembly pressure drop, heat-transfer coefficient, channel flow split, and total inlet liquid flow.

For the other parameters such as tube power fraction or tube thickness, which were considered to be less influential, we adopted the SRL-generated 2σ values (see Table I).[20] These uncertainty values were based on FLOWTRAN with a different set of LOCA conditions; thus, their applicability to a TRAC-based limit may be questionable. However, because of time limitations, we chose to use the SRL uncertainties and add additional uncertainty into our modeling bias factor.

We developed the modeling bias factor to account for uncertainties in the uncertainty analysis process and other modeling limitations This bias was set at 67% based on engineering judgment and is assumed to be a conservative multiplier on the nominal power less the 2σ uncertainty. The bias accounts for uncertainties resulting from

a. use of SRL FLOWTRAN uncertainties instead of TRAC-based uncertainties for certain influential parameters;
b. the "quasi-steady" link between the transient LOCA and the detailed assembly model;
c. hydraulic differences between the use of D_2O in the LOCA system calculation and H_2O in the detailed assembly model;
d. rib-flow assumptions, including the orientation of the inner target;
e. rod bow effects that were not addressed in the nominal model;
f. TRAC input data not validated through a formal quality assurance process;
g. incomplete understanding of physical processes; and
h. assumptions on annulus subchannel or sector flow splits and channel inlet modeling.

SRL adopted a 12% bias on assembly flow because of a 12% over-prediction of loop flows in the L-Area benchmarks. However, the TRAC L-Area benchmarks indicate that TRAC-computed loop flows agree well with experimental data and that TRAC-computed plenum levels are equal to or less than experimental measurements. In spite of this apparent ability of TRAC to predict L (or K) Reactor thermal-hydraulic behavior, we took (similar to SRL) a 5% core flow bias penalty to cover additional uncertainty in calculated assembly flows.

In the TRAC system calculation, we used the 1990 Sleeve Equation to arrive at assembly flows implicitly. The 1990 Sleeve Equation as presented by Durig[21] has a 2σ bound of 1.30 gal./min, which we included as a flow bias. In addition, as suggested by SRL, the plenum level was sensitive to RELAP-5 computed airflow, and thus, a 0.3-in. plenum level bias was applied. However, TRAC-computed airflows for the L-Area tests appear reasonable, and this penalty is not appropriate. Also, the B&W AFD and A-Tank test data show differences of up to 0.5 in. in plenum level for the same liquid flow. Similar to SRL, this difference was applied as a penalty on the TRAC-computed assembly flow.

When these additional penalties or biases are imposed on the TRAC-calculated nominal assembly flow, the resulting conservative assembly flow is 20.45 gal./min. The maximum assembly operating power (conservative estimate) then is computed as

$$P_{assembly} = P_{nominal} * (1-2\sigma) * b/dr \ ,$$

where

$P_{assembly}$	=	conservative estimate of maximum assembly operating power,
$P_{nominal}$	=	nominal ECS power limit from TRAC assembly calculation,
2σ	=	square root of sum of 2σ uncertainties = 0.39,
b	=	modeling bias = 0.67, and
dr	=	decay heat ratio at limiting time in PDLOCA transient = 0.0299.

VII. SUMMARY

Mark-22 assembly $T_{wall} = T_{sat}$ power limits were calculated using TRAC-PF1/MOD3. The calculated conservative estimate maximum assembly operating power, based on the calculated fuel wall temperature not exceeding the local saturation temperature translates to a nominal K Reactor power level equal to 37.6% of historical power based on the total number of effective assemblies. Figure 9 shows the TRAC-calculated nominal and conservative estimated power limits as a function of assembly liquid flow.

Several limited benchmark analyses were performed to evaluate model performance and build confidence in the use of the model. B&W AFD experiments were benchmarked with pressure gradient, axial void, and air entrainment data with generally favorable results. Rig FA $T_{wall} = T_{sat}$ data are compared with TRAC calculations in Fig. 10. In Fig. 11, the results of SPRIHTE data are compared with TRAC- and FLOWTRAN-calculated power limits for similar conditions. The TRAC calculations closely followed the experimental data.

In conclusion, we feel that the TRAC-calculated conservative estimate limit will bound the actual LOCA/ECS limit and supports K Reactor operation at 30% of historical reactor power. We base our conclusions on the use of a reasonable methodology coupled with a detailed three-dimensional TRAC model, the limited sensitivity studies coupled with the conservative modeling bias, and the favorable results of prototypical benchmarks.

Additional work is planned and needs to be done to improve this assembly power limit calculation and to reduce uncertainties. With additional effort, this modeling approach could be extended to the calculation of limits based on the onset of thermal excursion.

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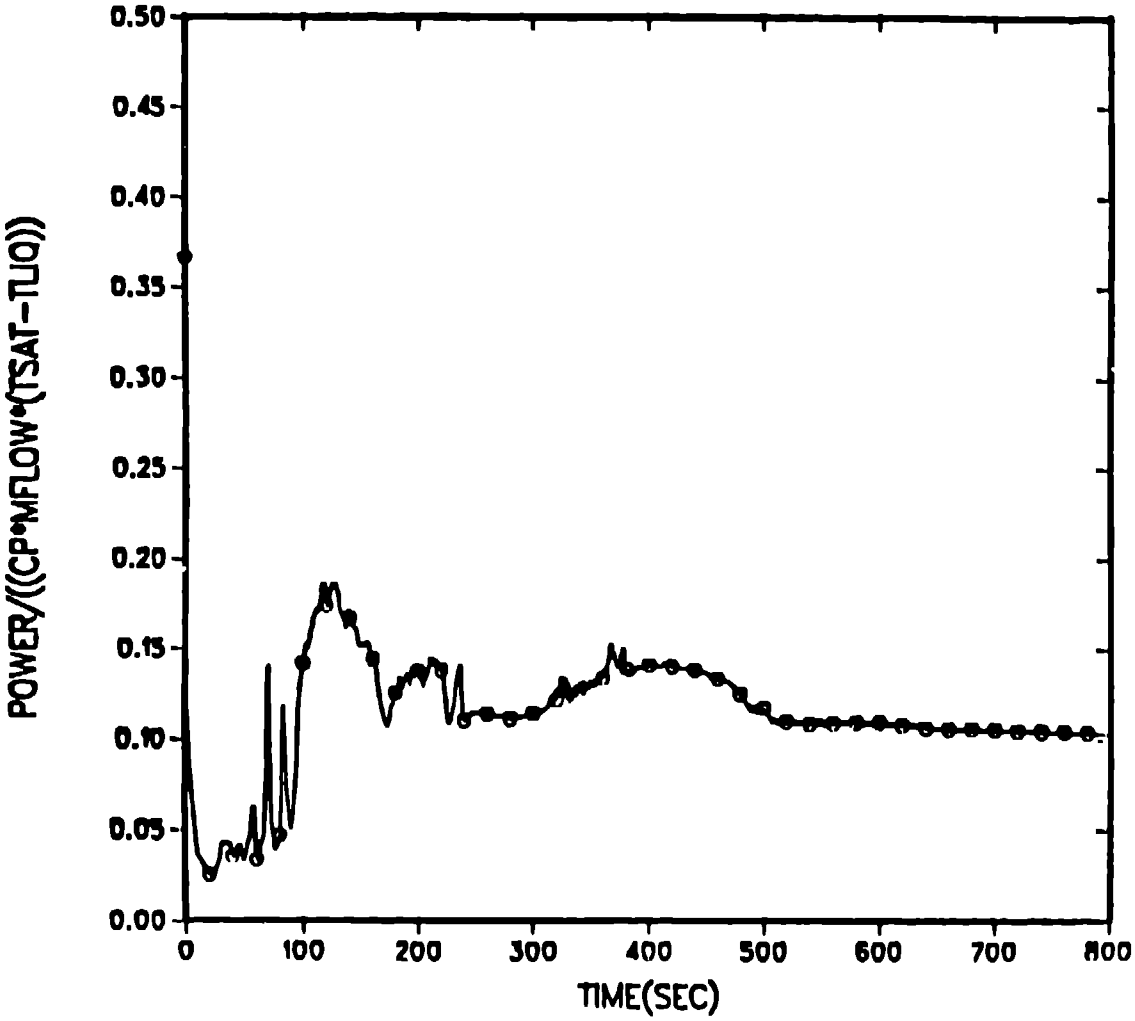

Fig. 1.
Power/CP*MFLOW*(T_{sat} - T_{liq}) vs time for limiting assembly from the TRAC PDLOCA calculation.

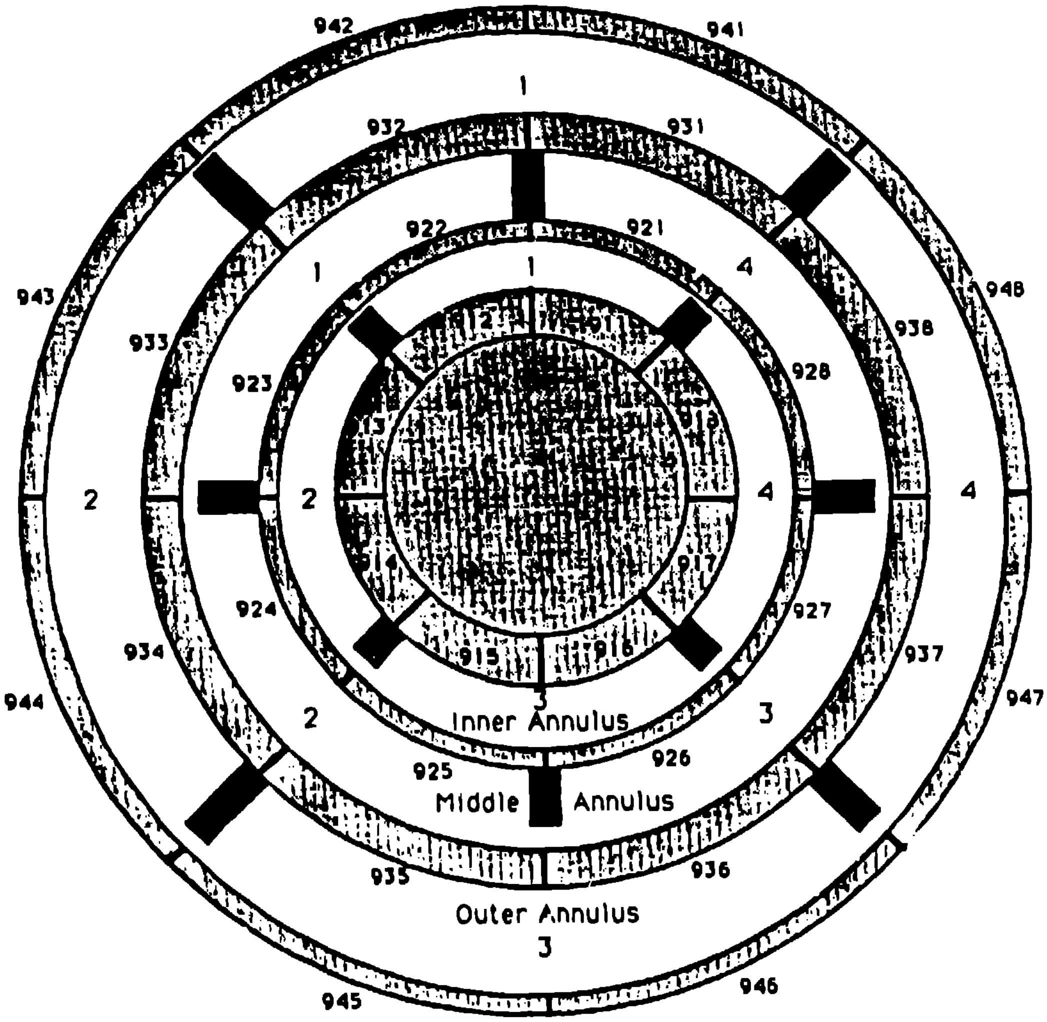

Fig. 2.
Schematic of TRAC assembly model showing VESSEL and HEAT STRUCTURE components.

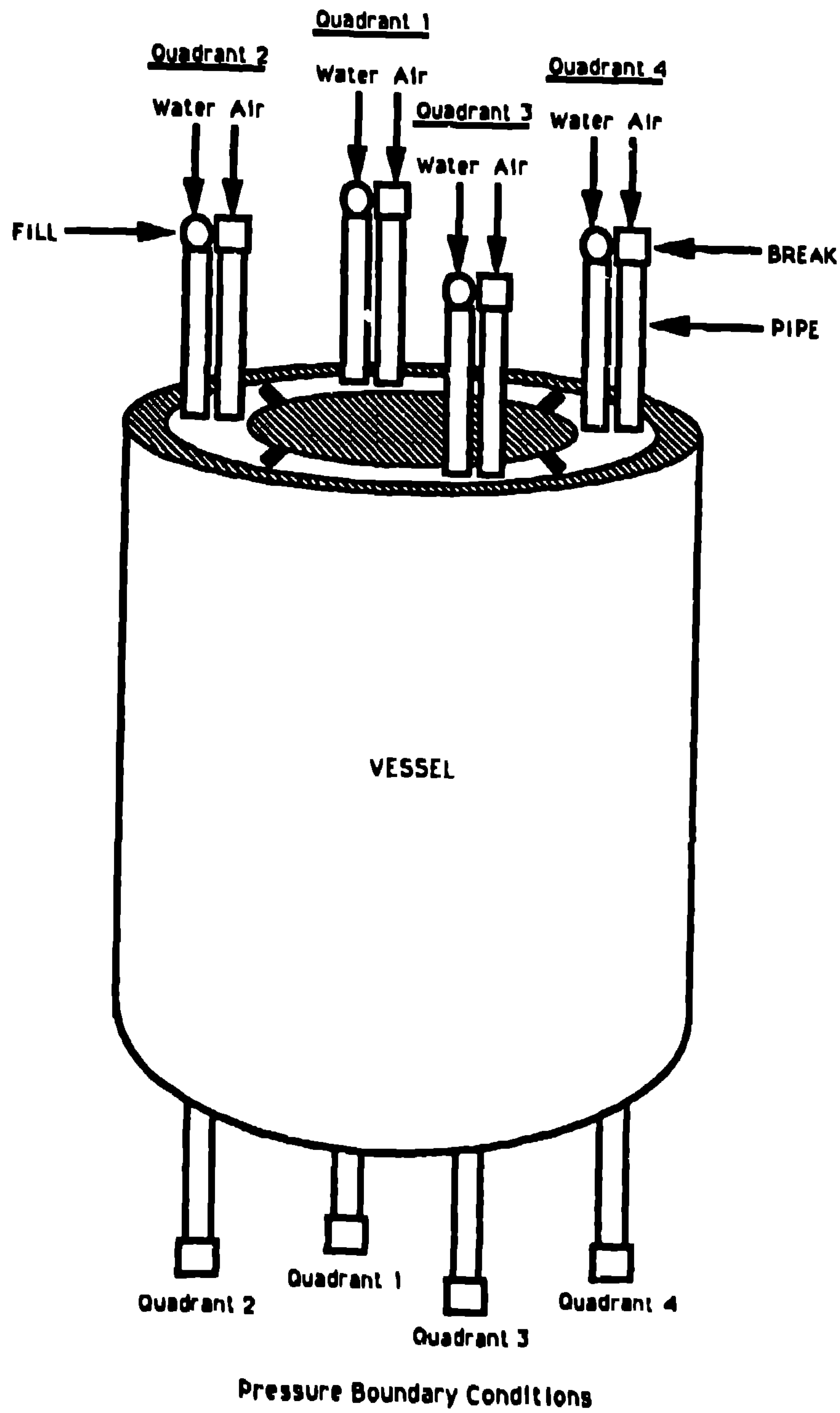

Fig. 3.
Schematic of the TRAC input model with boundary components used to represent an annulus of a Mark 22 assembly.

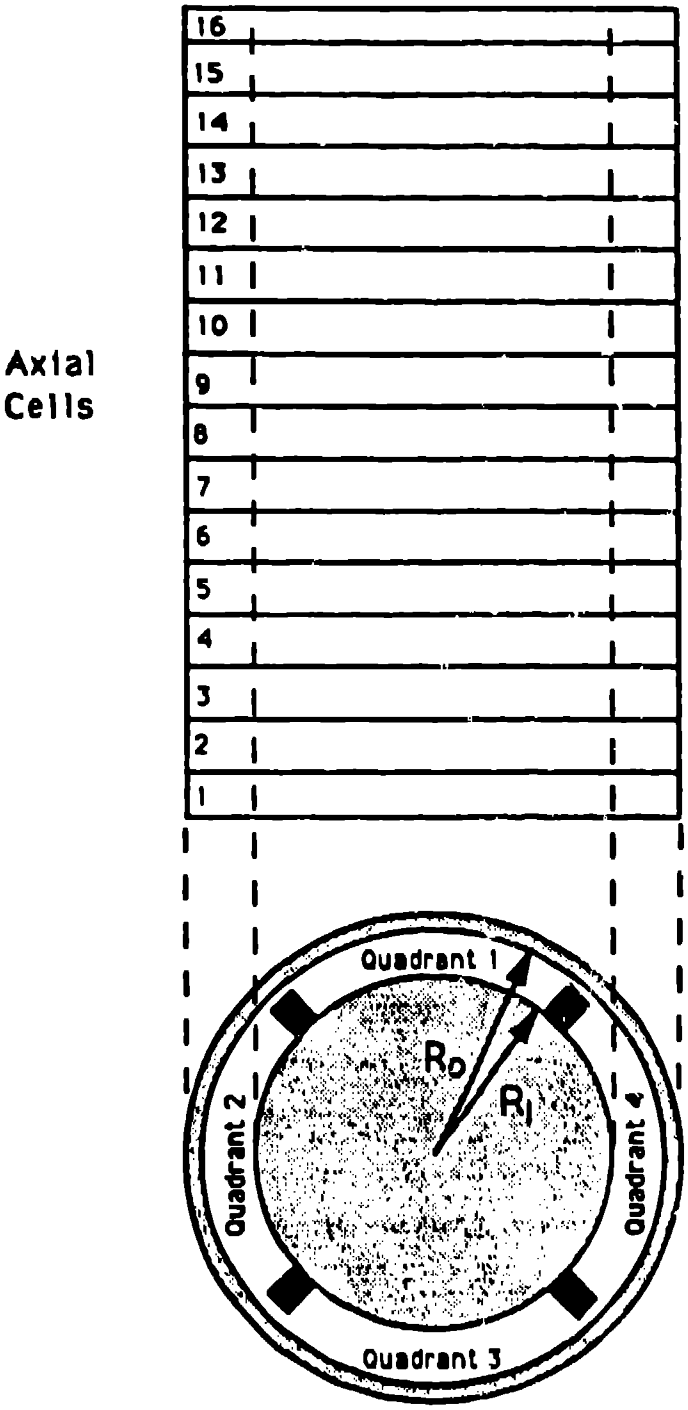

Fig. 4.
Schematic of the VESSEL component used to represent an annulus of the Mark 22 assembly.

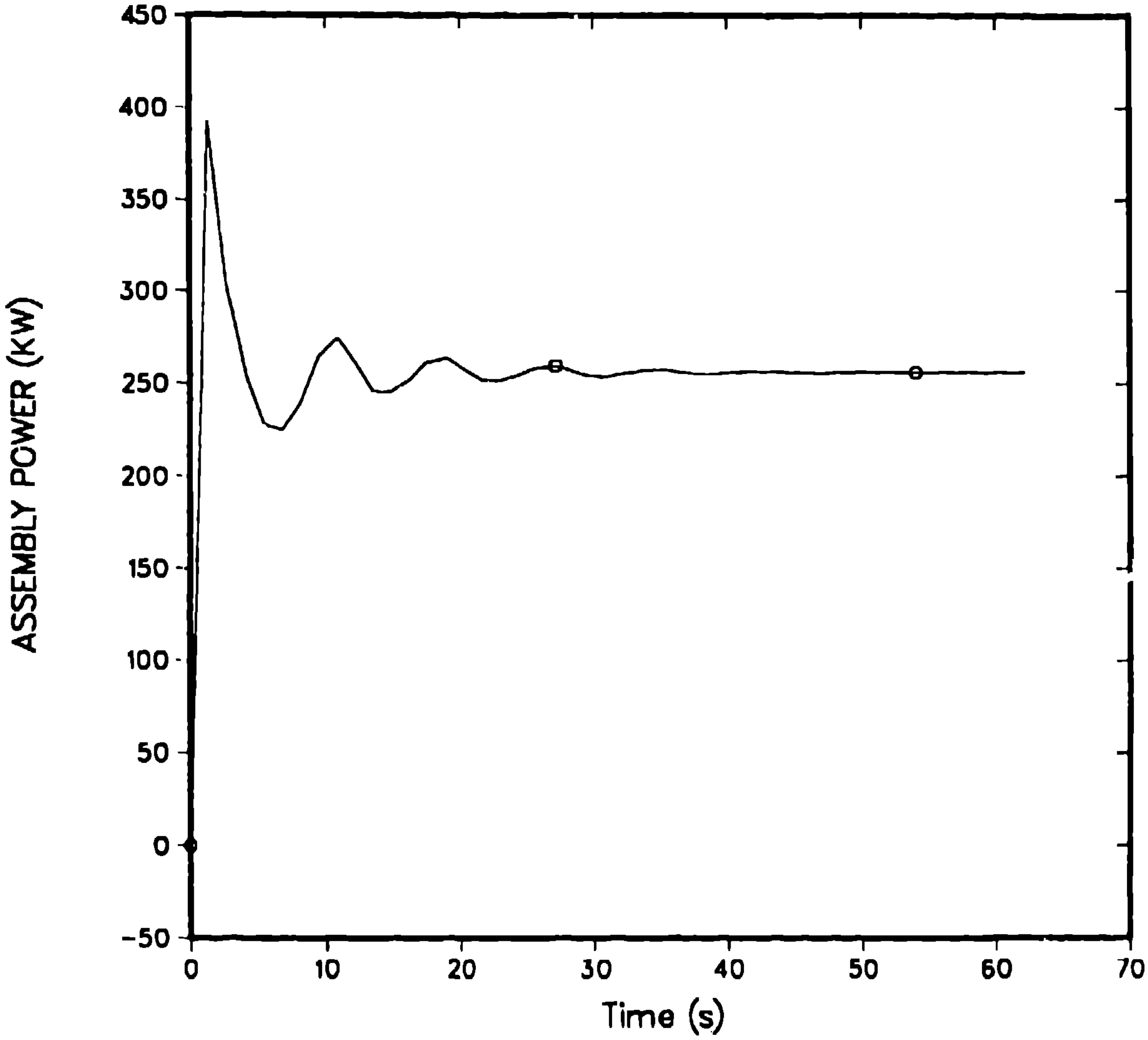

Fig. 5.
Typical plot of assembly power vs time using TRAC PID controller to achieve steady state.

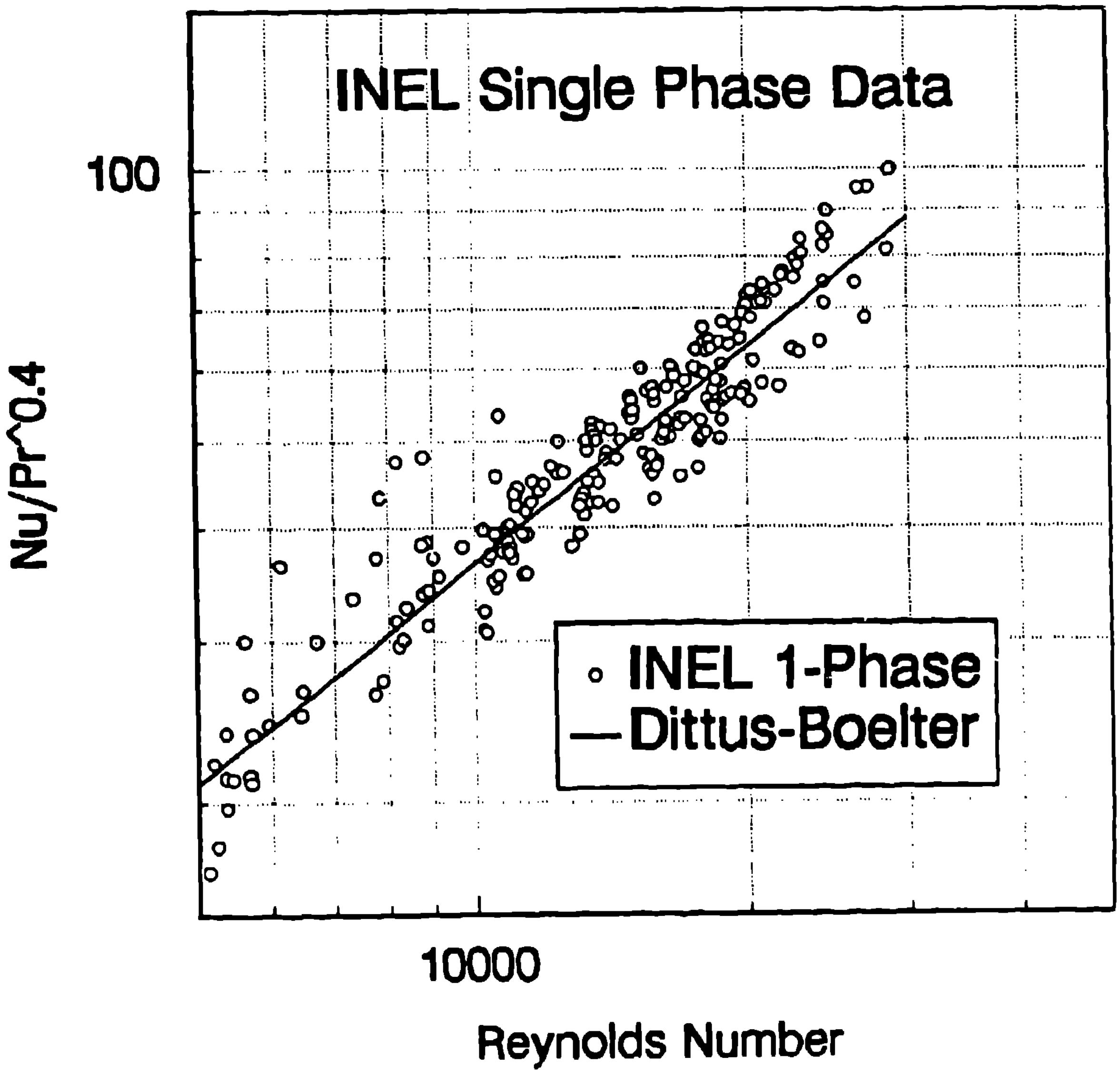

Fig. 6.
INEL single-phase heat-transfer data compared with Dittus-Boelter correlation.

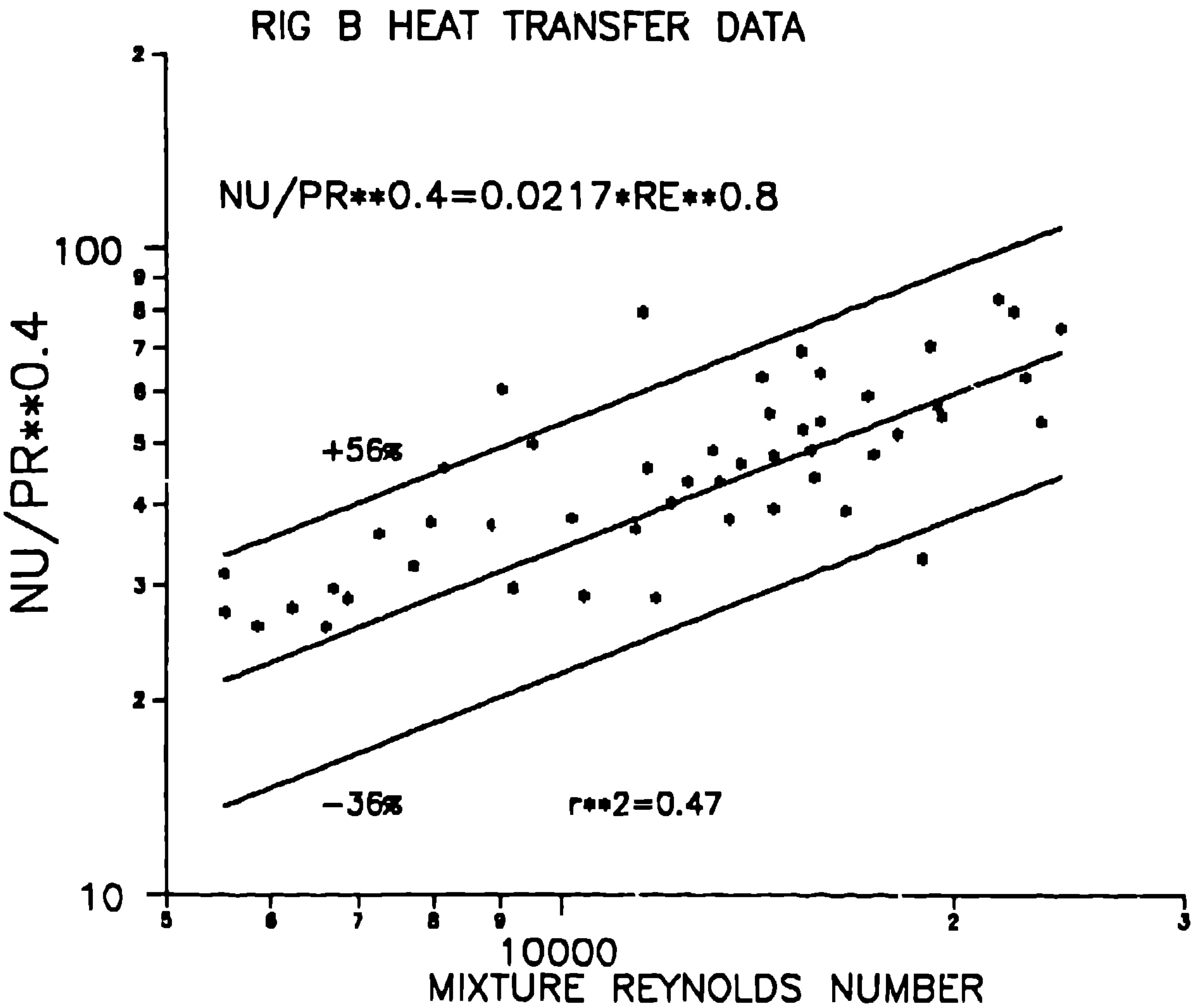

Fig. 7.
Plot of SRL heat-transfer correlation compared with Rig B heat-transfer data.

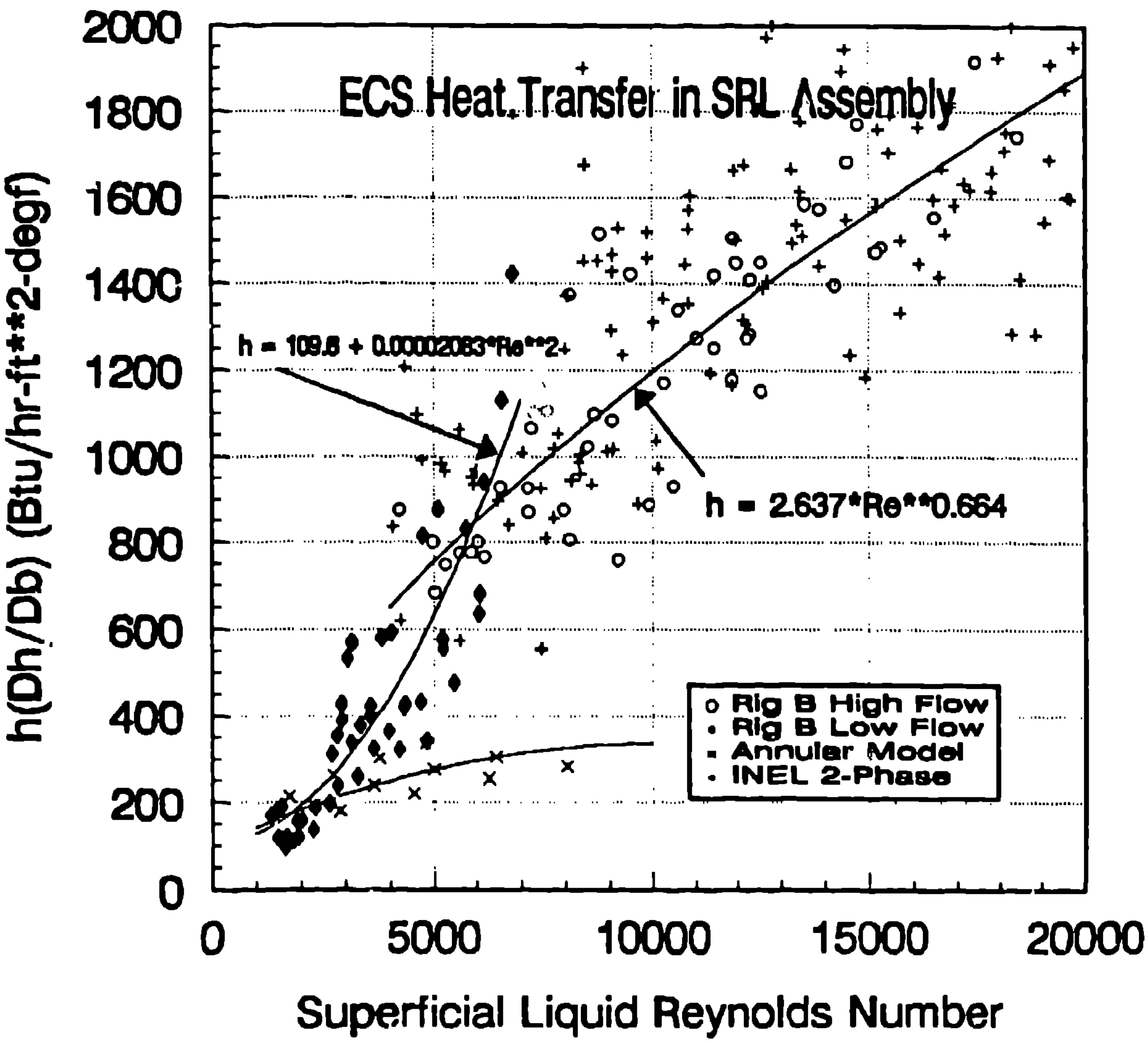

Fig. 8.
Los Alamos correlation compared with Rig B high- and low-flow data and INEL heat-transfer data.

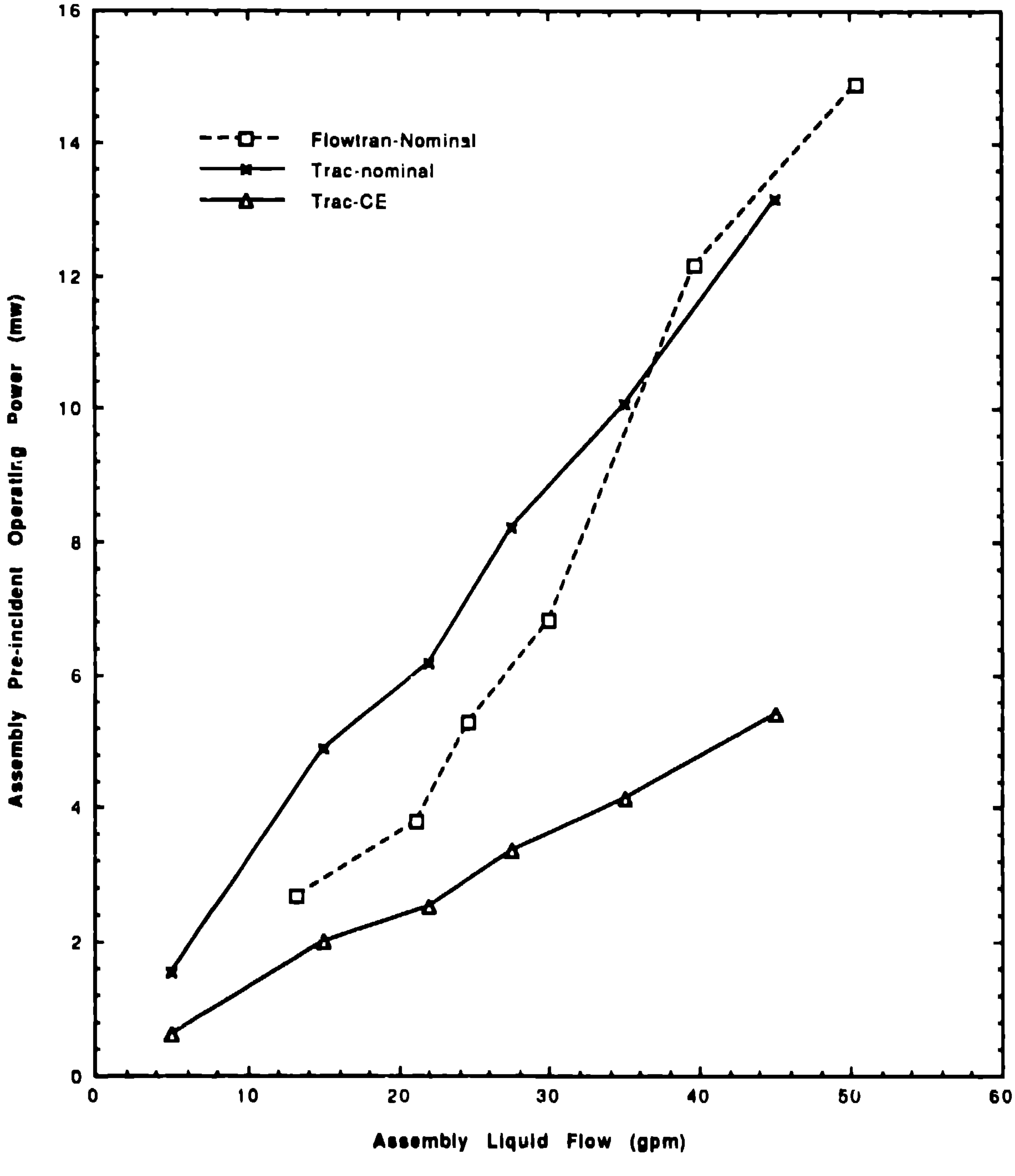

Fig. 9.
Mark-22 assembly Twall = Tsat power limit—sensitivity of power to flow at limiting LOCA conditions.

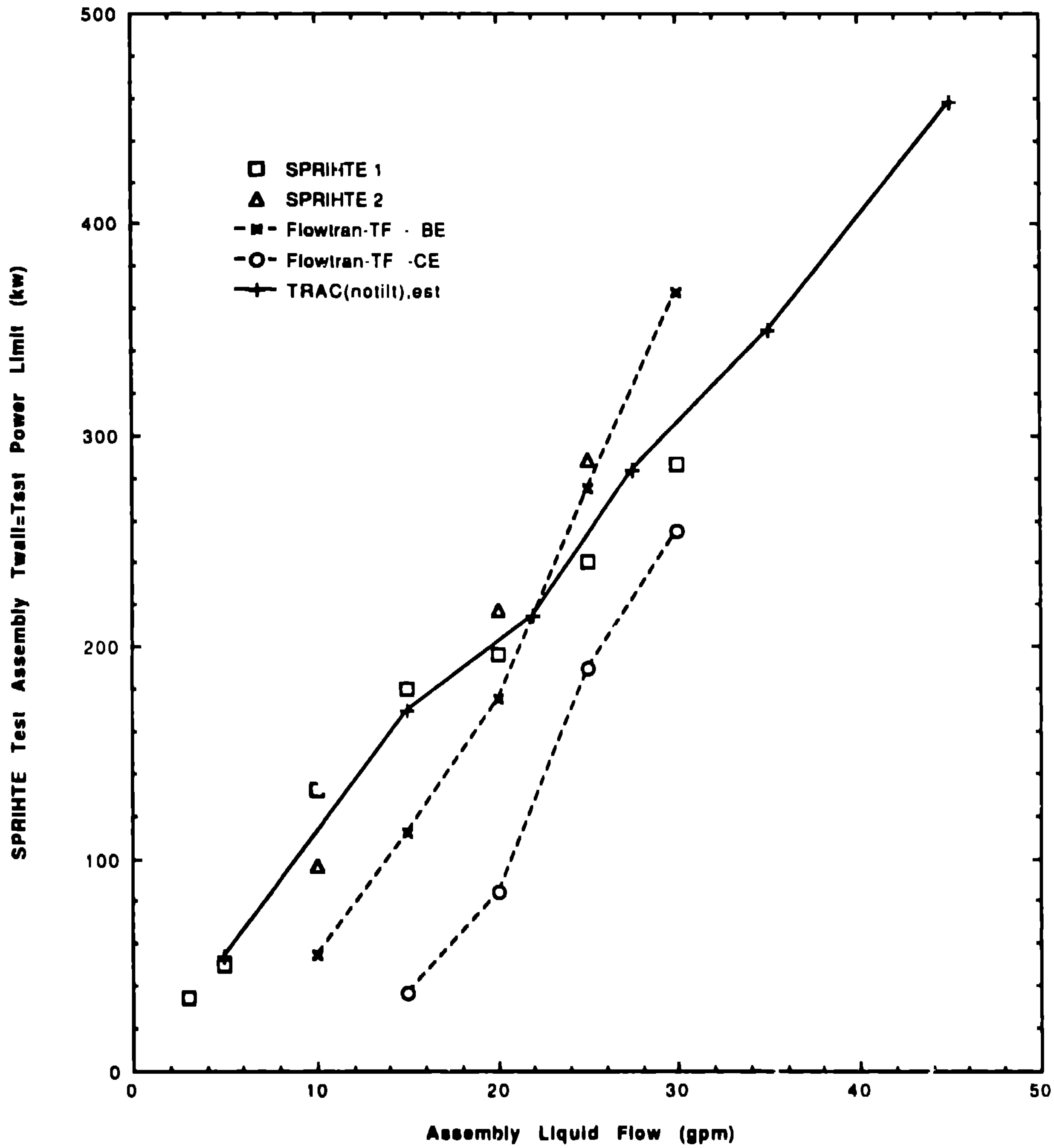

Fig. 10.
Rig FA Twall = Tsat power limits us flow—TRAC calculations compared with experimental data.

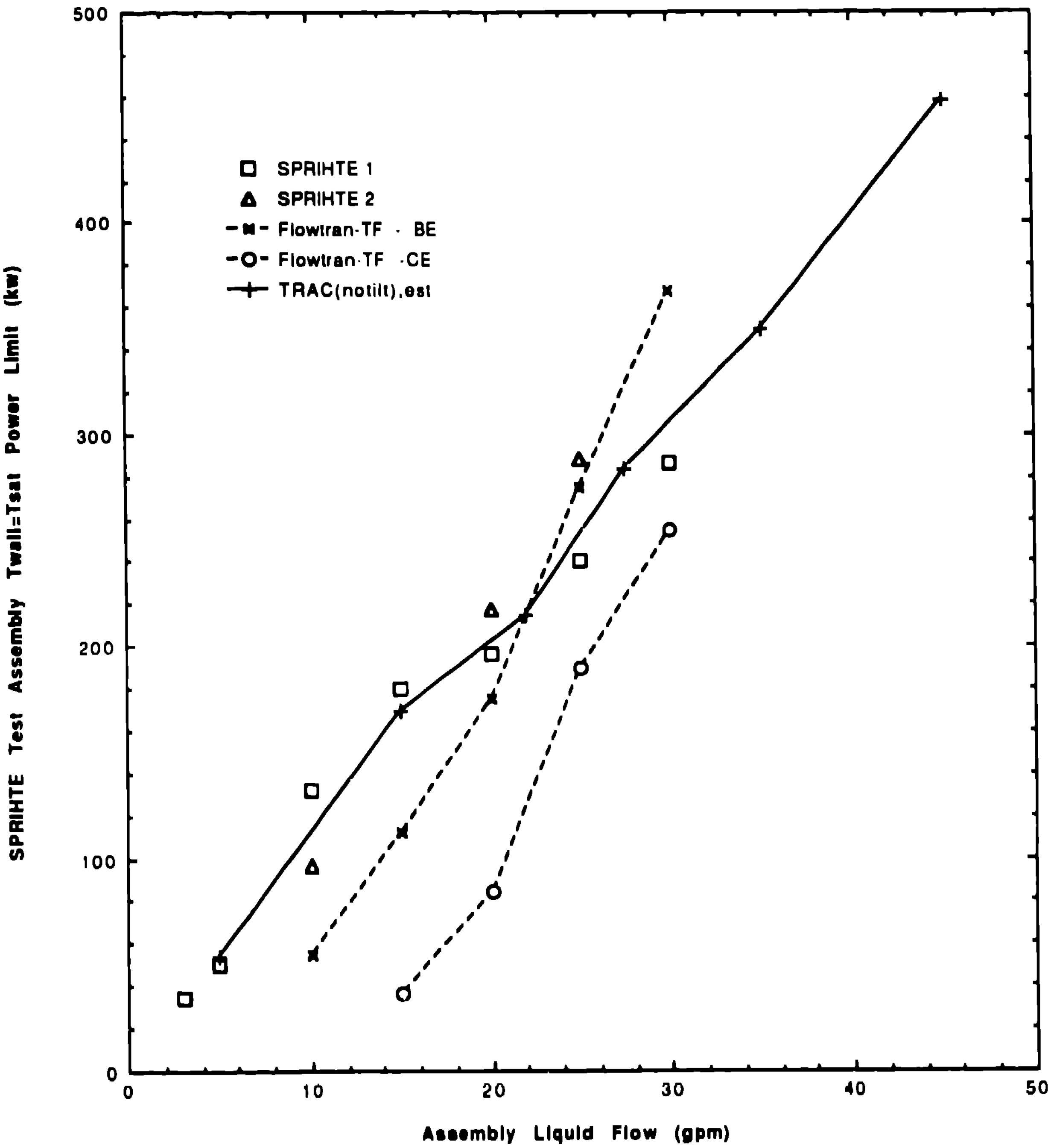

Fig. 11.
SPRIHTE $T_{wall} = T_{sat}$ power limits vs flow—TRAC calculations compared with experimental data.