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APPR-1 REACTOR TRANSIENT ANALYSIS

VOLUME I

BASIC KINETIC MODEL AND EQUATIONS

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1. - SUMMARY

A basic kinetic model of the primary system coolant loop is developed for the Army Package Power Reactor -1. Externally controlled variables are control rod insertion and steam generator power output. Equations are derived for solution by analog computer techniques, and include the following considerations:

1. Transport lag of coolant through primary system piping.
2. Core temperature and pressure reactivity coefficients.
3. Heat storage capacity of fuel plates, primary and secondary system liquid, and of steam generator tubes.
4. Five group delayed neutron contributions.
5. Portion of core power generated outside fuel plates.

The significant assumptions made in the derivation include:

1. Core considered as a lumped system, with fuel plates at a uniform mean temperature, and with a uniform coolant velocity distribution.
2. Steam generator considered as a lumped system, with a uniform shell-side temperature corresponding to saturation, and with a constant overall heat transfer coefficient.
3. Slug flow in piping, complete mixing in reactor vessel plenum chambers.

Refinements in the model, at the expense of increasing the complexity of the mathematical treatment and the associated analog circuitry, are contemplated. Future work will be published in additional volumes under the same APAE Memo number.

2. - INTRODUCTION

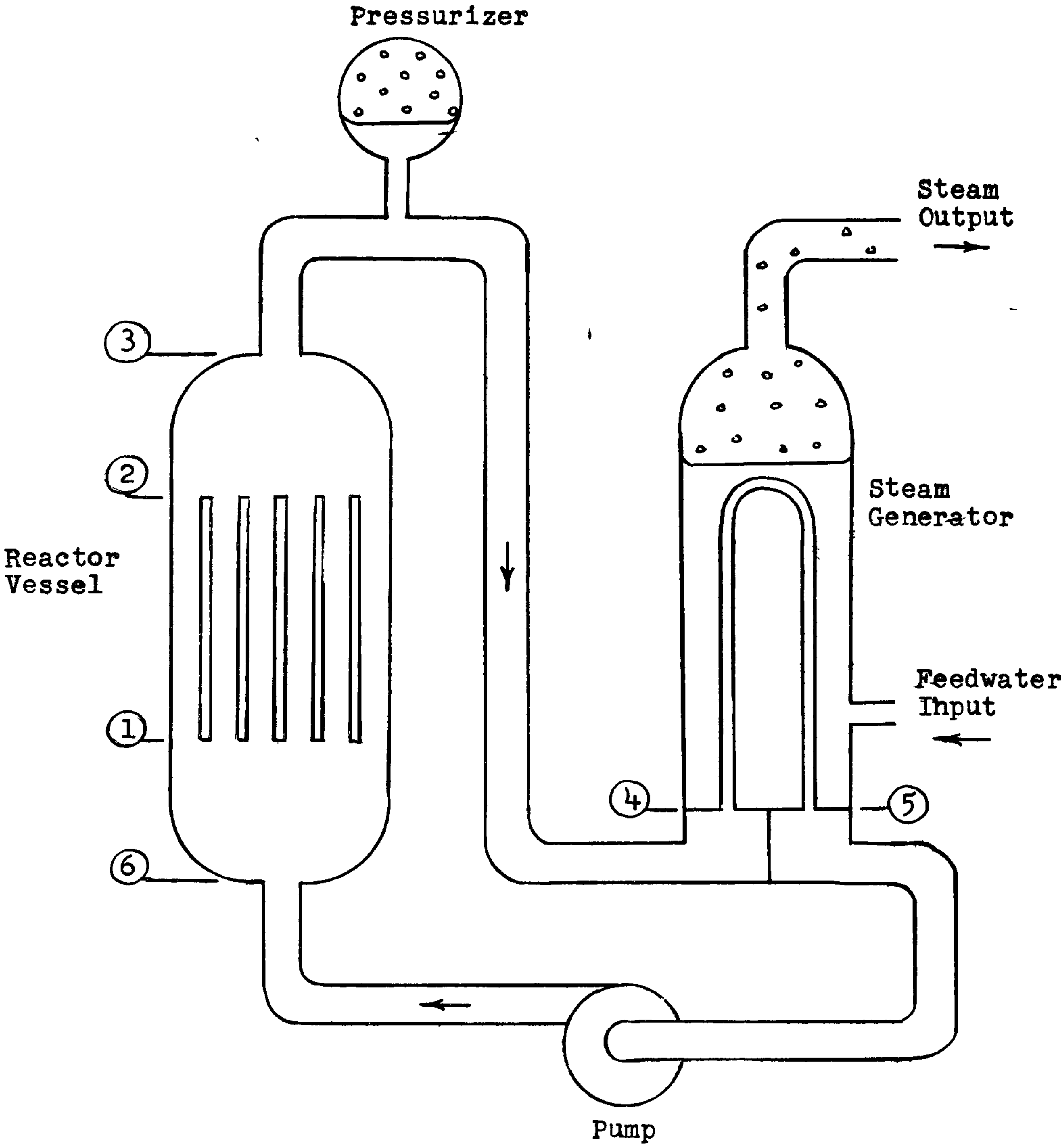
This report is the first of a series describing studies made under Research and Development Task #4 of the APPR-1 operating contract. The objective of this task is to establish a mathematical model of the APPR-1 reactor and power plant system which will respond to load and reactivity changes in essentially the same manner as the plant, and to use this model to determine plant behavior for off-design conditions. The existence of such a model will be useful in future design work and hazard analysis. The transient behavior data themselves are essential for the proper design of a training simulator.

The adequacy of the model will be determined by direct comparison of model and measured plant response to identical perturbations. Refinements in the analog representation will be made until a satisfactory corelation is achieved.

This report describes the basic model that will be the first used in the comparison. It is essentially similar to the models used in former transient studies on the APPR-1[(1)] and the APPR-1a[(2)]. Equation constants based upon plant dimensions have been revised to correspond with the final as-built design.

A schematic diagram of the model is presented in the following figure.

(1) "Derivation of the Thermal Kinetic Equations for the Package Reactor," J.G. Gallagher & M.L. Winton, ORNL 55-4-53, April, 1955.
(2) "Phase III Report, Army Package Power Reactor -1a, Design Analysis, Vol. I" APAE No. 17, May 1957.



SCHEMATIC DIAGRAM OF KINETIC MODEL

3. - NOMENCLATURE

3.1 - Symbols

C	Specific heat, Btu/lb °F
F	Prop. constant, neutron population/core power, neutron sec/Btu
K	Potential power contribution of delayed neutrons, M/F, Btu/sec
l	Mean neutron lifetime, birth to absorbtion, sec^{-1}
L	Load factor of steam generator power output
M	Potential neutrons of a delayed group, available upon decay
N	Neutron population in core
p	Primary system pressure, psia
P	Power output of core, Btu/sec
R	Rate of primary system flow, lb/sec
S	Slope of pressurizer characteristic, $\Delta p/\Delta V_{tot}$, psi/ft^3
t	Time, sec
T	Temperature, °F
V	Volume, ft^3
W	Weight, lb
α	Fraction of power generated in fuel plates and cladding
β	Delayed neutron fraction of a group
γ	Volume coefficient of primary coolant expansion, $°F^{-1}$
δ	Excess reactivity of core
$\mathcal{S}$	Excess reactivity coefficient, $°F^{-1}$ or $psia^{-1}$
θ	Temperature difference at design load, °F
λ	Decay constant of a delayed neutron group, sec^{-1}
τ	Time lag, sec

3.2 Subscripts

C	Mean core coolant condition
D	Design power output condition, steady state
E	Exchanger tubing
F	Mean fuel plate condition
G	Mean steam generator condition, primary (tube) side
i	i^{th} delayed neutron group
L	Liquid in steam generator
neg	Negative increase in primary coolant volume
p	Primary pressure
pos	Positive increase in primary coolant volume
r	Control rod insertion
S	Mean steam conditions in generator corresp. to saturation
tot	Total for primary system
T	Core temperature
1-8	Thermo. properties: Condition at location in schematic diagram
1-5	Nuclear parameters: Particular i^{th} delayed neutron group

4. - DERIVATIONS

4.1 - Thermal Kinetics of Core

The temperature distributions in the fuel plates and core coolant are difficult to express analytically because of heat generation and coolant velocity variations. While the use of an orifice plate across the core as in the APPR-1 adds to this difficulty, radial variations of fuel plate or of coolant temperatures are reduced.

For the purpose of performing a transient analysis of the primary loop, the following simplifying approximations are made regarding plate and coolant conditions.

1. In replacing the distributed system by a lumped system, fuel plates are considered to be at a uniform mean temperature, where the gradient between mean plate and mean coolant temperatures is proportional to the heat flow.
2. For the temperature magnitudes involved, log mean and arithmetic mean coolant temperatures are nearly equivalent, so the latter is used.
3. The coolant time through the core is small compared with the entire primary loop.

Performing a heat balance on the fuel plates of this model, letting storage rate equal generation rate minus transfer rate:

$$W_F C_F \frac{dT_F(t)}{dt} = \alpha P(t) - \frac{\alpha P_D}{\theta_{F,C}} \left[T_F(t) - T_C(t) \right]$$

$$\frac{dT_F(t)}{dt} = \frac{\alpha}{W_F C_F} P(t) - \frac{\alpha P_D}{W_F C_F \theta_{F,C}} T_F(t) + \frac{\alpha P_D}{W_F C_F \theta_{F,C}} T_C(t) \qquad (1)$$

The mean design-load temperature difference "$\theta_{F,C}$" can be estimated on the basis of the fuel plate power and surface area, and the average film coefficient corresponding to average core velocity.

That fraction of the core power not produced directly in the fuel plates can be considered as being produced wholly in the core coolant stream since only about 1-2% is produced outside the confines of the core skirt. A heat balance on the coolant flow through the core then yields:

$$W_C C_C \frac{dT_C(t)}{dt} = (1-\alpha)P(t) + \frac{\alpha P_D}{\theta_{F,C}} \left[T_F(t) - T_C(t) \right] - RC_C \left[T_2(t) - T_1(t) \right]$$

$$\frac{dT_C(t)}{dt} = \frac{1-\alpha}{W_C C_C} P(t) + \frac{\alpha P_D}{W_C C_C \theta_{F,C}} T_F(t) - \left[\frac{\alpha P_D}{W_C C_C \theta_{F,C}} + \frac{2R}{W_C} \right] T_C(t) + \frac{2R}{W_C} T_1(t)$$

The coefficients can be simplified by the following identities:

$$\frac{P_D}{W_C C_C} = \frac{RC_C \theta_{12}}{W_C C_C} = \frac{\theta_{12}}{W_C/R} = \frac{\theta_{12}}{\tau_C}$$

$$\frac{R}{W_C} = \frac{1}{W_C/R} = \frac{1}{\tau_C}$$

The final core coolant heat balance then becomes:

$$\frac{dT_C(t)}{dt} = \frac{1-\alpha}{W_C C_C} P(t) + \frac{\alpha \theta_{12}}{\tau_C \theta_{FC}} T_F(t) - \left[\frac{\alpha \theta_{12}}{\tau_C \theta_{FC}} + \frac{2}{\tau_C} \right] T_C(t) + \frac{2}{\tau_C} T_1(t) \qquad (2)$$

Since the value of "α" is nearly unity (about .94 for the APPR-1), a good approximation of eq. (1) and (2) is obtained by considering all power to be generated within the fuel plates. However, the previously defined value of "θ_{FC}" is still preferably used. Using a value of unity for α then, eq. (1) and (2) become:

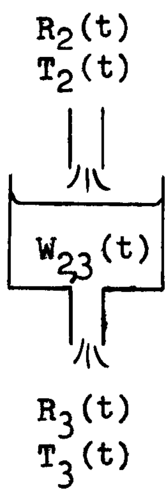
$$\frac{dF_F(t)}{dt} = \frac{1}{W_F C_F} P(t) - \frac{P_D}{W_F C_F \theta_{FC}} T_F(t) + \frac{P_D}{W_F C_F \theta_{FC}} T_C(t) \qquad (1')$$

$$\frac{dT_C(t)}{dt} = \frac{\theta_{12}}{\tau_C \theta_{FC}} T_F(t) - \left[\frac{\theta_{12}}{\tau_C \theta_{FC}} + \frac{2}{\tau_C} \right] T_C(t) + \frac{2}{\tau_C} T_1(t) \qquad (2')$$

4.2 - Kinetics of Plenum Chambers and Piping

There are essentially four plenum chambers in the primary loop. They are located at the core inlet and outlet and at the steam generator inlet and outlet. The latter two plenums are rather small, and can be considered as extensions of the piping.

The complex flow patterns in the reactor vessel plenums make the relationship between inlet and outlet temperatures difficult to express. However, because of the high degree of mixing that does occur, an approximation of this relationship is achieved by assuming the mixing to be complete. An analytical expression for outlet temperature as a function of inlet temperature is derived by means of an energy balance of the two plenum chambers:



$$R_2(t)T_2(t)C - R_3(t)T_3(t)C = C\,\frac{d[WT_3(t)]}{dt}$$

Conditions:

$$R_2(t) = R_3(t) = R \qquad \text{(Constant)}$$

$$W_{23}(t) = W_{23} \qquad \text{(Constant)}$$

Substituting:

$$R\,T_2(t) - R\,T_3(t) = W_{23}\,\frac{dT_3(t)}{dt}$$

$$\frac{dT_3(t)}{dt} = \frac{1}{\tau_{23}}\,T_2(t) - \frac{1}{\tau_{23}}\,T_3(t) \tag{3}$$

Similarly for the core entrance plenum:

$$\frac{dT_1(t)}{dt} = \frac{1}{\tau_{61}}\,T_6(t) - \frac{1}{\tau_{61}}\,T_1(t) \tag{4}$$

If T_2 were a constant, then the differential equation (3) can easily be solved analytically:

$$\frac{dT_3(t)}{dt} + \frac{1}{\tau_{23}} = \frac{T_2}{\tau_{23}}$$

Homogeneous solution: $T_3(t) = Ke^{\frac{-t}{\tau_{23}}}$

Particular solution: $T_3(t) = T_2$

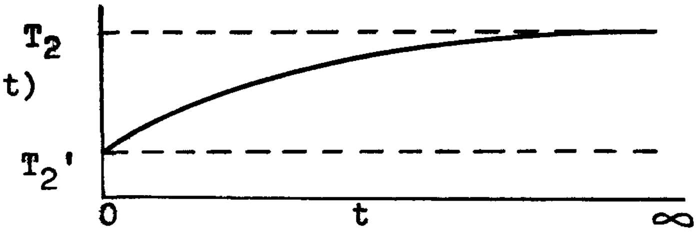
General solution: $T_3(t) = Ke^{\frac{-t}{\tau_{23}}} + T_2$

If T_2 has undergone a step increase, from T_2' to T_2, then:

Boundary condition: $T_3(t) = T_2'$ at $t = 0$

Arbitrary constant: $K = T_2' - T_2$

Final equation: $T_3(t) = (T_2' - T_2)e^{\frac{-t}{\tau_{23}}} + T_2$



However, in the simulation of reactor transients, input temperatures T_2 and T_6 are continuously varying, so that the more general equations (3) and (4) must be used.

Fluid flow in ducts exhibits a non-uniform velocity profile across the diameter, but at the high Reynolds number of the primary piping the profile is essentially flat, and mathematical relationships of the transport lag are based upon slug flow.

$$T_4(t - \tau_{3,4}) = T_3(t) \qquad \text{where } \tau_{3,4} = W_{3,4}/R \qquad (5)$$

$$T_6(t - \tau_{5,6}) = T_5(t) \qquad \text{where } \tau_{5,6} = W_{5,6}/R \qquad (6)$$

By combining the first order lag of the core exit plenum, eq. (3), with the transport lag of connected piping, eq. (5), the steam generator inlet temperature can now be expressed in terms of the core exit temperature.

$$\frac{dT_3(t)}{dt} = \frac{1}{\tau_{2,3}} T_2(t) - \frac{1}{\tau_{2,3}} T_3(t) \qquad (3)$$

$$\frac{dT_4(t+\tau_{3,4})}{dt} = \frac{1}{\tau_{2,3}} T_2(t) - \frac{1}{\tau_{2,3}} T_4(t+\tau_{3,4})$$

$$\frac{dT_4(t+\tau_{3,4})}{dt} = \frac{2}{\tau_{2,3}} T_C(t) - \frac{1}{\tau_{2,3}} T_1(t) - \frac{1}{\tau_{2,3}} T_4(t+\tau_{3,4}) \qquad (7)$$

Similarly, the core inlet temperature is expressed as a function of the steam generator exit temperature by combining eq. (4) and (6).

$$\frac{dT_1(t+\tau_{5,6})}{dt} = \frac{1}{\tau_{6,1}} T_6(t+\tau_{5,6}) - \frac{1}{\tau_{6,1}} T_1(t+\tau_{5,6}) \qquad (4)$$

$$\frac{dT_1(t+\tau_{5,6})}{dt} = \frac{1}{\tau_{6,1}} T_5(t) - \frac{1}{\tau_{6,1}} T_1(t+\tau_{5,6})$$

$$\frac{dT_1(t-\tau_{5,6})}{dt} = \frac{2}{\tau_{6,1}} T_G(t) - \frac{1}{\tau_{6,1}} T_4(t) - \frac{1}{\tau_{6,1}} T_1(t+\tau_{5,6}) \qquad (8)$$

4.3 - Thermal Kinetics of Steam Generator

The steam generator is the most difficult reactor component to express kinetically. This report considers a simplified generator model employed in earlier transient analysis studies of the APPR-1 and APPR-1a. The errors introduced by these simplifying generator approximations on the overall reactor kinetics are believed to be greater than the errors introduced by previous component approximations. If an improvement in reactor simulation is warranted at the price of increased complexity in mathematical relationships and in analog circuitry, then attention should first be placed on the generator model.

The following assumptions are made in this treatment of the steam generator:

1. The primary time lag through the tubes is small compared with the loop lag.
2. The thermal capacity of the tubes is lumped with that of the secondary fluid.
3. The heat transfer across the tubes varies linearly with the difference between the mean primary temperature and the secondary saturation temperature.
4. The log mean and arithmetic mean primary temperatures are nearly equivalent under the generator conditions, so the latter is used.

A heat balance on the tube side fluid under the above assumptions yields:

$$W_G C_C \frac{dT_G(t)}{dt} = RC_C \left[T_4(t) - T_5(t) \right] - \frac{P_D}{\theta_{G,S}} \left[T_G(t) - T_S(t) \right]$$

$$\frac{dT_G(t)}{dt} = \frac{2R}{W_G} \left[T_4(t) - T_G(t) \right] - \frac{P_D}{W_G C_C \theta_{G,S}} \left[T_G(t) - T_S(t) \right]$$

The coefficients can be simplified as in Section 4.1:

$$\frac{dT_G(t)}{dt} = \frac{2}{\tau_G} \left[T_4(t) - T_G(t) \right] - \frac{\theta_{12}}{\tau_G \, \theta_{G,S}} \left[T_G(t) - T_S(t) \right] \quad (9)$$

The feedwater rate is controlled to equal the steam rate. Therefore, except for poor control, the combined weight of the shell side liquid and steam remains fixed. Very small variations in the phase proportions will occur with pressure to satisfy saturation equilibrium, but the liquid weight "W_L" is essentially constant. Performing then a heat balance on the shell side fluid:

$$\left[W_L C_L - W_E C_E\right] \frac{dT_S(t)}{dt} = \frac{P_D}{\theta_{GS}} \left[T_G(t) - T_S(t)\right] - P_D L(t)$$

$$\frac{dT_S(t)}{dt} = \frac{P_D}{(W_L C_L + W_E C_E)\,\theta_{GS}} T_G(t) - \frac{P_D}{(W_L C_L + W_E C_E)\,\theta_{GS}} T_S(t) - \frac{P_D}{W_L C_L + W_E C_E} L(t) \quad (10$$

Since the variation in steam enthalpy is small over the secondary pressure range encountered, and since the feedwater temperature is about constant, power output is nearly proportional to the steam rate. The output term "L(t)" of eq. (10) is quite general, however, and need not be based on such approximations.

4.4 - Nuclear Kinetics of Core

The neutron population of the APPR-1 core is maintained by the fission of U-235 atoms. On an average basis, 2.1 neutrons are liberated for every neutron absorbed in the fuel. Most of the liberated neutrons are released within 10^{-14} seconds or less after fission and are called "prompt neutrons". As the average neutron lifetime from birth to absorbtion is in the order of 10^{-5} seconds, the prompt neutron delay time can be considered negligible in the kinetic equations. However, for U-235, 0.75% of the liberated neutrons are released after longer time intervals and are called "delayed neutrons". They can be divided into five major groups, each of which is characterized by its own unstable source and a corresponding decay constant.

At steady state conditions, only one of the 2.1 neutrons liberated above is reabsorbed in the fuel. The neutron population therefore remains constant. The remainder is absorbed by structural or other substances, or may leak out of the core. If the relative number of wasted neutrons is reduced, the population will increase because of the excess reactivity "δ" introduced. The excess reactivity is the fraction of the neutron generation rate not being balanced by absorbtion and leakage.

Consider the simplified case of a core with no delayed neutrons. If an excess reactivity situation existed, the neutron population "N" in the core would increase exponentially according to the relationship:

$$\frac{dN(t)}{dt} = \frac{\delta}{l} N(t) \qquad \text{or} \qquad N(t) = N_0 e^{\delta l/t}$$

The net value of the excess reactivity is the sum of individual contributions effected by rod position, core coolant temperature, and core pressure.

$$\frac{dN(t)}{dt} = \frac{\delta_r - \delta_T - \delta_p}{l} N(t)$$

The omission of delayed neutron effects introduces errors into a transient analysis. For greater accuracy, five groups are considered. The improvement in utilizing the six group theory is too small to be warranted.

Excess reactivity components are derived under steady state core conditions, the component sum being unity, and therefore include delayed neutron generation. The total delayed fraction must be subtracted to determine prompt power with a separate power term added to the equation for the delayed neutron contributions.

$$\frac{dN(t)}{dt} = \frac{(\delta_r + \delta_T + \delta_p) - \sum_{i=1}^{5} \beta_i}{l} N(t) + \sum_{i=1}^{5} \lambda_i M_i(t)$$

where $$\frac{dM_i(t)}{dt} = \frac{\beta_i}{l} N(t) - \lambda_i M_i(t)$$

Core power is closely proportional to the neutron population. The following relationships are therefore used to replace neutron terms by power terms:

$$N(t) = F\,P(t) \qquad \delta_T = \mathcal{J}_T\left[T_C(t) - T_C(0)\right]$$

$$\frac{dN(t)}{dt} = F\frac{dP(t)}{dt} \qquad \delta_p = \mathcal{J}_p\left[p(t) - p_D\right]$$

$$\frac{M_i(t)}{F} = K_i(t)$$

The neutron population equation can then be rewritten:

$$\frac{dP(t)}{dt} = \frac{\delta_r - \mathcal{J}_T\left[T_C(t)-T_C(0)\right] - \mathcal{J}_p\left[p(t)-p_D\right] - \sum_{i=1}^{5}\beta_i}{l}\,P(t) + \sum_{i=1}^{5}\lambda_i K_i(t) \qquad (11)$$

where

$$\frac{dK_i(t)}{dt} = \frac{\beta_i}{l}\,P(t) - \lambda_i K_i(t) \qquad (12)$$

4.5 - Pressurizer Kinetics

Equation (11) includes the effect of primary system pressure changes on the core reactivity. Although this effect is relatively small, it has been incorporated in the core kinetics for greater accuracy. Primary pressure changes are determined by the relationship between primary volume changes and the pressurizer characteristics.

An accurate estimate of overall system volume change is obtained by the summation of individual changes of coolant portions around the loop as follows:

$$\Delta V_{tot} = \gamma \left[V_C \, \Delta T_C(t) + V_{2,3} \, \Delta T_3(t) + V_{3,4} \, \frac{\Delta T_3(t) + \Delta T_4(t)}{2} + V_G \, \Delta T_G(t) + V_{5,6} \frac{\Delta T_5(t) + \Delta T_6(t)}{2} + V_{6,1} \Delta T_1(t) \right] \quad (13)$$

However, since the pressure effect on core reactivity is small, a less exacting summation is used in order to simplify the associated analog circuitry.

$$\Delta V_{tot} = \gamma \left[V_C \, \Delta T_C(t) + (V_{2,3} + V_{3,4}) \, \Delta T_4(t) + V_G \, \Delta T_G(t) + (V_{5,6} + V_{6,1}) \, \Delta T_1(t) \right]$$

$$\Delta V_{tot} = \gamma V_C T_C(t) + \gamma V_{2,4} T_4(t) + \gamma V_G T_G(t) + \gamma V_{5,1} T_1(t) - \gamma \left[V_C T_C(0) + V_{2,4} T_4(0) + V_G T_G(0) + V_{5,1} T_1(0) \right] \quad (14)$$

The pressurizer kinetic equations are difficult to express accurately because of the following characteristics. An increase in primary volume causes the pressurizer liquid level to rise, compressing the steam pocket. Because the heat transfer rate from the now superheated steam to the pressurizer walls and the liquid is small, the compression is essentially adiabatic and therefore reversible back to the starting liquid level.

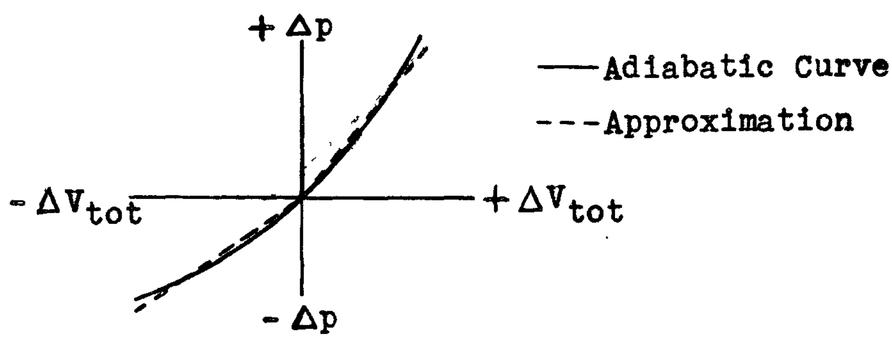
A decrease in the primary volume causes the pressurizer liquid level to fall, tending to expand the steam pocket below its former saturation value. Flashing of the liquid will therefore occur to maintain phase equilibrium. Because such flashing absorbs heat from the liquid and reduces its temperature, the steam pressure will decrease, but at a slower rate than had the expansion been adiabatic. Should the liquid now rise back to the starting level, the steam compression will be essentially adiabatic as outlined in the previous paragraph, and will not reversibly follow the expansion relationships but end at a higher pressure.

The decrease in pressure may have been sufficient to actuate the pressurizer-liquid heaters. A low pressure transient would be significantly affected by heater operation over a time interval of a few minutes.

If the pressurizer riser had been fitted with a checkvalve and spray nozzle combination such that incoming liquid associated with a primary volume increase is sprayed into the steam pocket, then phase equilibrium within the pressurizer would be maintained over the full pressure range. Such additional complexity of the plant equipment was not justified on an operational basis, however, and it would be wishful thinking to expect its incorporation to simplify the transient analysis.

However, since the effect of pressure change on the core reactivity is relatively small, great accuracy in expressing the pressurizer characteristics can be sacrificed while still maintaing high accuracy on overall plant kinetics. The following approximations are therefore made to simplify analog circuitry:

1. Reversible adiabatic expansion and compression of the steam pocket is assumed. This is conservative since a given drop in primary volume will yield a larger than actual pressure decrease, thus presenting a pessimistic picture of pressure swings.
2. The adiabatic curve is approximated by two straight lines, extending from the normal operating point to the extremes of pressure variations encountered, as illustrated in the diagram below:



The pressurizer characteristics are therefore expressed in the form:

$$p(t) - p_D = S_{pos} \Delta V_{tot}(t) \quad , \Delta V_{tot} > 0 \qquad (15)$$

$$p(t) - p_D = S_{neg} \Delta V_{tot}(t) \quad , \Delta V_{tot} < 0 \qquad (16)$$

5. - EVALUATION OF CONSTANTS

5.1 - Fluid & Metal Volumes

Reactor vessel inlet plenum, active	29.4	ft^3
exit plenum	24.7	
core passages	4.05	
Piping, reactor to generator, active	8.11	
, generator to reactor, short leg	16.8	
, long leg	* 21.9	
Pump, each of two, active	4.60	
Steam generator inlet plenum	3.20	
exit plenum	4.31	
tubing passages	11.63	
liquid, shell side	51.6	
steam, incl. separator loop	73.9	
tubing above tube sheet	5.04	
Fuel and side cladding, excl. inert ends	0.914	

5.2 - Densities, & Flow Rates

Average density, primary liquid	51.8	lb/ft^3
, secondary liquid	54.3	
, steam generator tubing	493	
, fuel and side cladding	483	

Primary flow rate, short route pump	3810	GPM
, long route pump	* 3960	GPM
	8.82	ft^3/sec
	457	lb/sec

*This pump and associated leg used in transient study

5.3 - Equation Constants

C_C	1.094	V_{23}	24.7
C_E	0.12	V_{34}	11.3
C_F	0.109	V_{56}	26.5
C_L	1.03	V_{61}	29.4
l	2.0×10^{-5}	W_C	210
P_D	1200	W_E	2490
P_D	9483	W_F	442
R	457	W_G	602
S_{pos}	75.25	W_L	2802
S_{neg}	45.81	W_{23}	1279
T_{CD}	440.8	W_{34}	586
T_{FD}	461.2	W_{56}	1373
T_{SD}	381.8	W_{61}	1523
T_{1D}	431.6	α	0.94
T_{2D}	450.0	β_1	0.85×10^{-3}
V_C	4.05	β_2	2.41×10^{-3}
V_G	11.63	β_3	2.13×10^{-3}

β_4	1.66×10^{-3}	λ_3	0.151
β_5	0.25×10^{-3}	λ_4	0.0315
γ	9.35×10^{-4}	λ_5	0.0124
δ_p	0.73×10^{-6}	τ_C	0.459
δ_T	-2.4×10^{-4}	τ_G	1.319
$\theta_{F,C}$	20.4	$\tau_{2,3}$	2.80
$\theta_{G,S}$	59.0	$\tau_{3,4}$	1.281
$\theta_{1,2}$	18.4	$\tau_{5,6}$	3.00
λ_1	1.61	$\tau_{6,1}$	3.33
λ_2	0.456		