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AECU-3349

PHYSICS

UNITED STATES ATOMIC ENERGY COMMISSION

GAS PRESSURIZATION OF HOMOGENEOUS
REACTOR TEST

February 14, 1956

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Technical Information Service Extension, Oak Ridge, Tenn.

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metadc100772

Appendices Declassified: August 17, 1956.

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GAS PRESSURIZATION OF
HOMOGENEOUS REACTOR TEST

Appendices A through G

Report to
OAK RIDGE NATIONAL LABORATORY

February 14, 1956
C-60034

Work performed under Contract No. W-7405-Eng-26

ARTHUR D. LITTLE, INC.

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I. SUMMARY

A. CONCLUSIONS AND RECOMMENDATIONS

1. Oxygen is recommended as the most suitable gas for pressurization because it is:
 - (a) chemically compatible with both the core and blanket materials,
 - (b) readily available commercially and easily produced at any plant site if desired,
 - (c) safely handled in commercially available equipment, and
 - (d) easily disposed of by chemical reaction with materials such as ordinary hydrogen.
2. Continuous injection and venting of oxygen at the pressurizer is recommended as the most practical means of controlling explosive mixture concentration in the pressurizers when gas pressurization is used. It will be necessary to establish the rate of absorption of oxygen pressurizing gas in a mock-up experimental test.
3. Although there is some uncertainty as to the explosive limits of D_2 - O_2 - D_2O mixtures at the temperature and pressures involved, it is possible to hold the D_2 concentration at such a low value in the pressurizers that the pressurizer vessels can be built to resist any explosion which might occur. We recommend that for normal operating conditions, the D_2 concentration be held to 0.75% by volume. Under severe abnormal conditions (i.e., pump stoppage in the blanket side), it is conceivable that this concentration could rise to 3.25% before corrective steps could be taken. The proposed design of the pressurizer will resist an explosion of this concentration.
4. Gas can be continuously vented from the pressurizers by the arrangement of the vent connection to allow passage of the vent gas to a point of slightly reduced pressure in the high-pressure loop. By this arrangement, the pressure differential across the vent-gas flow-control device will be about 23 psi, and the severe burden of high-pressure reduction will be carried by the present main letdown valve without the appreciable alteration of the conditions under which the valve has to operate.

5. We propose a dump procedure which involves delaying the discharge of liquids to allow catalytic recombination of deuterium and oxygen to take place so that explosive mixtures do not occur in the system during dump. The extent to which the dump must be delayed is partly dependent upon whether mass boiling occurs. It is recommended that the extent to which mass boiling occurs be determined experimentally in dump tests on the Homogeneous Reactor Test (HRT). Thus it can be established how long the dump must be delayed to allow for catalytic recombination of deuterium and oxygen.
6. Pressures on the core and blanket sides can be equalized most safely by an open equalizer line between the tops of the two pressurizers. If a good entrainment separator is provided in each pressurizer, there should be no difficulty with cross-contamination of the core and blanket liquids except under most extreme conditions.
7. It is recommended that the residual oxygen from the oxygen injection be disposed of by reaction with ordinary hydrogen in a second recombiner downstream of the present heavy-water cold-trap. This allows a great deal of flexibility in the selection of the amount of oxygen to be injected to maintain pressure and dilute radiolytic deuterium below explosive limits. In addition, the recombiner facilitates rapid reduction of pressure to atmospheric throughout the entire system after an emergency dump.
8. The safety and operating capacity of the charcoal fission-gas adsorption beds can be substantially improved by the use of ordinary hydrogen instead of oxygen as the carrier gas. This can be accomplished by the control of hydrogen addition at the second recombiner recommended under (5) so that a small residual-hydrogen flow is made available for passage through the charcoal beds.
9. Mechanical design of the high-pressure core-and-blanket fluid-circulating equipment to confine explosions which might occur under abnormal operating conditions appears extremely difficult. Only a small departure from normal operating procedures can lead to a D_2 concentration of 20% in any free gas evolved in this section of the equipment, and the explosion pressure from such a concentration is too high to be confined by any practical method.
10. Detailed calculations, engineering flow sheets, preliminary component designs, and pertinent engineering data required as a basis of detailed design are presented in Appendixes A to G.

II. INTRODUCTION

To prevent boiling and to reduce the actual volume of radiolytic gas within the core and blanket of an aqueous homogeneous reactor, one must hold the pressure of the core-and-blanket circuits considerably above the vapor pressure of water at the operating temperature. The HRT high-pressure circuits are designed for a normal pressure of 2000 psi, which is 750 psi in excess of the vapor pressure of water at the 300C maximum operating temperature within the core.

Early design plans for the HRT called for maintenance of this over-pressure by the use of electrically heated pressurizers in which small amounts of core and blanket liquid would be held at about 335C to provide a cushion of vapor at 2000 psi. Early mock-up tests disclosed, however, that severe corrosion and scale-formation problems were associated with the heating of the core liquid to this temperature. As a result, two parallel attacks were directed toward the development of a more suitable pressurizer. One approach was to modify the steam-pressurization system (electrically heated steam pressurize) so that the steam is generated by electrically heating distilled heavy water out of contact with the fuel solution rather than by directly heating the fuel and blanket solutions. The other approach was directed toward the preliminary design of a method to maintain the over-pressure and to provide a cushion by the injection of a gas, thereby avoiding the need to raise the temperature above 300C. The work carried out under the second approach is the subject of this report.

There are a number of problems associated with the design of a gas-pressurization system, most of which are common to any pressurization system whether it involves steam or non condensable gas as the pressurizing medium. We here present a brief outline of the many factors which must be considered in the design of a gas-pressurization system.

A suitable pressurization system must meet the following requirements:

1. It must provide a controlled pressure of 2000 psi on the system to prevent boiling and minimize the volume of entrained gas in the core-and-blanket fluids.
2. It must provide a gas cushion of sufficient volume into which the fluid may surge in the event of sudden thermal expansion of the liquid within the core. In the HRT such an excursion could result in expulsion of as much as 10 liters of liquid from the core in 0.1 seconds. The pressurizer system must provide a gas cushion against which this amount of liquid can surge with only a moderate increase in pressure.

3. Since the core and blanket are separated by a thin Zircaloy core vessel, and since accidental filling of the blanket space with core liquid would cause serious difficulties including shut down, the core vessel must be protected against excessive pressure differential. The maximum permissible pressure differential between the pressurizers is specified as 200 psi, and a differential during normal operations of less than 50 psi is desirable. These requirements for pressure equalization must be met during starting and stopping procedures, as well as during normal operations. The maximum of 200 should never occur, except during brief intervals of an emergency.
4. Cross-contamination of the core and blanket liquids must be avoided. Thus, the pressure-equalizing system must be designed to prevent passage of entrained liquid from one system to the other. On the other hand, it is permissible for heavy-water vapor to pass from one system to the other.
5. The pressurizer system must accommodate means for measuring liquid level in order that the level of liquid in both the core and blanket loops can be controlled. Present plans call for the control of levels by means of modulating letdown valves which are governed by liquid-level controllers operated from level indicators installed in the pressurizers. These letdown valves control the rate of discharge of gas and liquid from the separators in the core and blanket loops.
6. The pressurizing system must be designed and operated so that dangerous explosive mixtures of radiolytic gas cannot build up in the pressurizers at any time during normal procedures for starting, running, or stopping, and emergency procedures for stopping ("dumping"). In normal operation, radiolytic decomposition of heavy water occurs in both the core and blanket systems, and substantial quantities of D_2 and O_2 in stoichiometric proportion are produced. Thus the core and blanket fluids are not only saturated with dissolved D_2 and O_2 but also contain suspended gas amounting to approximately 1% of the volume of the core and blanket fluids. This suspended gas is continuously separated from the circulating liquids in the gas-liquid separators and discharged along with fission gases and a small fraction of the circulating liquids to the low-pressure systems through the letdown valves. A catalyst promotes recombination of the dissolved D_2 and O_2 within the high-pressure circuits. At the high recombination rates thus obtainable at maximum core temperatures (no load condition), the D_2 and O_2 content can be reduced very quickly by catalytic recombination.

7. The pressurizing gas should have no adverse effect on the core or blanket fluids, or add to the corrosion or contamination problems of the system.
8. Provisions must be made in the design of the over-all system for the safe disposal of effluent gas resulting from the addition of pressurizing gas to the system. Disposal must be effective both under normal conditions of operation and on the occasion of an emergency dump. Gas phase catalytic recombiners are provided in the low-pressure systems to recombine the residual radiolytic D_2 and O_2 . The venting of fission gases to the atmosphere is delayed to allow for decay to a safe level. Charcoal adsorption-beds are provided for this purpose, but the flow capacity of these beds is limited. If large amounts of pressurizer gas must be injected into the pressurizer system to insure safe dilution of the D_2 , consideration must be given to the disposal of this gas. Moreover, in an emergency dump, it is necessary to first discharge the fluids from the high-pressure loops and then reduce the residual-gas pressure in both the high- and low-pressure systems to atmospheric level within one hour. With gas pressurization, provision must be made for the disposal of the charge of gas from the pressurizers at much higher rates following a dump than during normal operation.
9. Since the over-all objective of the homogeneous reactor test program is to establish the commercial feasibility of this method of power generation, continuity of operation and safety are of great importance. Confinement of all radioactive materials within the process circuits therefore becomes a vital requirement, because gross spillage of such materials by rupture of the equipment would entail prolonged decontamination periods with resultant lost time from power production. In general, if the process cannot be controlled to absolutely prevent formation of explosive mixtures, the equipment should be designed to contain the explosion.
10. The importance of continuity of operation noted under (9) makes reliability in operation one of the very important design goals.

The problem of gas pressurization involves far more than simply the design of the pressurizers alone. Consideration must be given to the problems of the over-all process in order that the method of pressurization may be well-integrated into the entire system.

III. INDIRECT GAS-PRESSURIZATION

Integration of a gas-pressurization system into the homogeneous reactor test would be simplified if some form of nonpermeable flexible barrier could be maintained between the gas and the core and blanket liquids. The problems of explosive mixture formation in the pressurizer, loss of pressurizer gas by solution in the liquid, and final disposal of the pressurizer gas would all be eliminated. In view of these possible advantages, several ways of using flexible metallic bellows, diaphragms, and liquid seals were considered briefly. Discussion of several of the more interesting methods is presented in Appendix A. We soon concluded, however, that application of any system of this type would require an extended development period because of the intricate mechanical and chemical problems associated with the production of a reliable barrier for this service. Since it was required that the chosen system should be feasible of application by mid-1956, these possibilities were quickly ruled out.

IV. DIRECT GAS-PRESSURIZATION WITH RECIRCULATION

From a process design standpoint, direct gas-pressurization with recirculation offers an outstanding degree of simplicity. Such a system would incorporate a multistage compressor to take all gas from the present condensers in the low-pressure system and compress it to about 2500 psi so that it could be reinjected into the pressurizers. Fission gases would be recirculated continuously, and over a long period of operation, might build up in concentration sufficiently to serve as the major constituent of the pressurizing gas. Provided the present injection rates for oxygen could be held low enough so that all injected oxygen was consumed by chemical reaction in the system, the amount of residual gas requiring a special disposal system such as the charcoal beds would become very small. The compressor used for recirculation would also serve as the compressor for charging disposable high-pressure bottles, or fixed-high-pressure hold tanks. It would also serve to pump out the residual gas following an emergency dump so that the gas could be stored in a hold tank during maintenance work. Use of such a system would conceivably eliminate the need for any cold traps or charcoal beds and the accessory equipment required by them. It would also facilitate the use of high gas-injection rates which would contribute to the safety of the system. Control of the system would be straightforward and simple.

The disadvantage of such a system is that it requires a fairly complex compressor under highly radioactive conditions. A diaphragm compressor like the Corblin, but with modification to make it suitable for use under radioactive conditions, should be practical. The only reason for not recommending such a system for application to HRT is that time-consuming mechanical development work would be required. This is not a system on which reliance could be placed as early as mid-1956. This system should prove attractive for later use, however, particularly for large-scale installations.

V. DIRECT GAS-PRESSURIZATION WITHOUT RECIRCULATION

Design for direct gas-pressurization involves consideration of all the process and operational factors noted in the Introduction. This makes selection of the pressurizing gas a complex study in itself.

A. SELECTION OF GAS

Since radiolytic decomposition of the heavy water in both the core and blanket liquids necessarily brings D_2 and O_2 into the system, a basic simplicity would be achieved if one or the other of these gases were used for pressurization. In principle, a neat and simple system can be devised on the basis of pressurizing with deuterium. Heavy water could be decomposed in a high-pressure electrolysis cell at 2500 psi and the deuterium collected in a high-pressure ballast tank at about 2400 psi, from which it would be fed into the pressurizers. The amount of oxygen formed in the electrolysis cell would be just sufficient for the complete recombination of the deuterium after it had progressed through the system to the present recombiner. The only unusual component of equipment needed for this process would be a high-pressure electrolysis cell. Such cells are described in the literature as being in commercial use in Europe. Although this system has several appealing aspects, we feel that it is not worthy of adoption because accidental or inadvertent departure from normal operating procedures could lead to more serious explosion hazards than by any other system. The amount of deuterium, and therefore the amount of energy available by explosion, would be greatly increased. There would be the additional hazard that gas leaks could lead to the formation of an explosive mixture outside of the equipment inside the reactor cell. The use of oxygen as a pressurizing gas does not involve these additional hazards, and it is therefore preferable to deuterium.

Of the commonly used inert gases, nitrogen and carbon dioxide must be ruled out because of reactivity under homogeneous reactor conditions. Both helium and argon, however, are known to be chemically acceptable. These gases are expensive (\$7.55 to \$11.00 per 100 SCF) and are in such great demand today that commercial supplies are seriously short. There would be no choice but to ship helium into a reactor site, because it is available in practical quantities only from a few natural gas deposits where it occurs in concentrations of a few percent. Argon, present in air to concentrations of a little under 1%, could be produced at the reactor site by fractionation of air. It is ordinarily made only as a by-product of oxygen production, and as a primary product, would be far more expensive to produce than oxygen. Both helium and argon are so inert that it is not practical

to dispose of them by chemical combination to form a solid or an easily condensed vapor. They would thus be difficult to dispose of quickly in the event of an emergency dump. Probably the most feasible way to deal with them would be by compression into a hold tank, but this would require the use of a special compressor suitable for handling highly-radioactive gases. At best, the use of helium or argon for pressurization involves a considerable amount of operating expense and logistic inconvenience.

Of the gases which are chemically acceptable, oxygen is the most readily available. It can be produced at the reactor site, either by electrolysis or by fractionation of air, with commercially proven equipment. In all industrial areas, compressed oxygen is readily available commercially and is reasonable in price (\$1.45 per 100 SCF). It is regularly produced in several grades, and quality standards are rigidly controlled. We recommend the "technical" grade because of its low-moisture and low-impurity content (-70F dewpoint and 99.9% O₂). As a practical matter, selection of oxygen incurs no additional safety hazard since the precautions necessary for safe handling are so well known. A full line of safe, commercially proven equipment for the handling of oxygen is available.

In addition to these practical factors, oxygen can be easily disposed of by chemical recombination. A neat process arrangement would be the counterpart of the system suggested earlier for use of deuterium. Heavy water would be electrolyzed and the oxygen would be injected into the pressurizers. Deuterium from the electrolysis cell would be directed to the present recombiner so that all of the oxygen injected for pressurization would be recombined within the recombiner. For use on HRT, however, it would seem desirable to avoid the burden of having to operate an electrolytic cell. Purchase of compressed oxygen is recommended as the most practical method of oxygen supply for HRT.

If purchased oxygen were used as the pressurizing gas, a second recombiner for disposal of the residual oxygen would be desirable. Purchased, compressed hydrogen would be used in this recombiner and the system would be arranged to prevent back-flow and consequent contamination of the deuterium with ordinary hydrogen. This use of ordinary hydrogen is recommended to provide a very flexible means for disposing of residual oxygen, and to provide a controlled flow of a preferred carrier gas through the charcoal adsorption-beds. By this method, the adsorption beds could be operated with a fixed, optimum flow of carrier gas, regardless of the rate of oxygen injection to the pressurizers or the rate of oxygen disposal after an emergency dump. Plenty of oxygen could then be injected to make certain that dangerous concentrations of deuterium do not develop in the pressurizers. In addition, at the conclusion of an emergency dump, when

it is necessary to dispose of 154 cu ft STP or more of oxygen from the normal inventory of gas within the pressurizers, this system makes it feasible to reduce pressure in the entire system to atmospheric within an hour.

B. EXPLOSION HAZARD

Of prime consideration in the design of a pressurizing system for HRT is the explosion hazard which may be involved if radiolytic gas finds its way into the pressurizer. This explosion hazard is common to any pressurizing system, whether it is steam or gas. Since radiolytic gases will be present in stoichiometric proportions, explosive recombination of these gases always is a possibility. A comprehensive treatment of this specific subject is given by T. H. Pigford. (1)

Further survey of the literature yielded a variety of information on the explosive limits of hydrogen in air and hydrogen in oxygen. Lange (2) gives a lower explosive limit of 4.1% hydrogen in air at atmospheric pressure and an auto-ignition temperature of 580C. The National Fire Protection Association (3) presents a corresponding figure of 4.1% hydrogen in air at atmospheric pressure as the lower explosive limit and an auto-ignition temperature of 585C.

Bone and Townsend (4) present data on explosive limits as a function of pressure. The lower limits of hydrogen in air are given as 9.0% at one atm, 9.5-9.9% at 10 atm, 9.9% at 50 atm, and 10% at 125 atm. These figures are considerably higher than the lower limits reported by Lange and the National Fire Protection Association, and indicate that the experiments upon which these figures are based are for downward propagation of the flame, which accounts for the higher limits. For example, in another experiment, the lower limit was found to be 4.15% H₂ in air for upward propagation, 6.50% H₂ in air for horizontal propagation and 8.8% H₂ for downward propagation. There appears to be little difference between lower limits for hydrogen in air and hydrogen in oxygen. Bone and Townsend report lower limits for downward flame-propagation at normal pressure and temperature of 9.5% H₂ in air and 9.2% H₂ in oxygen.

The effect of temperature on the explosive limits of hydrogen in air is also discussed by Bone and Townsend. The data was developed for downward propagation of the flame at atmospheric pressure. The Bureau of Mines (5) also presents some data on the influence of temperature on the explosive limits of hydrogen in oxygen for downward flame-propagation:

(1) See Bibliography, Appendix G.

<u>Temperature °C</u>	<u>% H₂ in Air</u>	
	<u>Bone and Townsend</u>	<u>Bureau of Mines</u>
15		9.6
17	9.4	
100	8.8	
200	7.9	
300	7.1	9.1
400	6.3	

The temperature effect would seem to have less effect in oxygen than in air.

Bone and Townsend also report data on the effect of pressure on the lower explosive limits of hydrogen in air for downward flame propagation.

<u>Pressure Atmospheres</u>	<u>% H₂ in Air</u>
1	9.0
10	9.5 - 9.9
50	9.9
125	10.0

The Bureau of Mines investigated the influence of pressure on the lower explosive limits of hydrogen in oxygen, presumably for downward flame propagation, and found that from 1 to 122 atm, the lower limits were between 8-9%.

Very little information was found in the general literature on the combined effect of temperature and pressure. Bone and Townsend report some data on auto-ignition by adiabatic compression of hydrogen and oxygen mixtures. A mixture of 9% H₂ ignited at 473C is the most recent experiment reported. The corresponding pressure was not reported but is readily computed as 22 atm, assuming that the gases were initially at 70F and 1 atm, and that compression was adiabatic and reversible. The data reported on atmospheric auto-ignition temperatures by Lange and the National Fire Protection Association, when compared with the results on auto-ignition by adiabatic compression, would seem to indicate that the ignition temperature of a given mixture might be seriously reduced at the pressures contemplated for the HRT. No data were found in the general literature in which the combined effects of high temperature and high pressure were investigated.

The most severe conditions expected in the HRT are 2000 psia (137 atm) and 300C. Although this is below the lowest auto-ignition temperature reported, it is sufficiently high to be considered hazardous. Moreover, Von Elbe (6) estimates that under these conditions, a bubble 10 in. or more in diameter is likely to ignite by a self-accelerating gas-phase reaction.

When the lower explosive limits at the conditions under consideration are estimated, it is assumed that deuterium will act identically with hydrogen, since there is no apparent reason to believe that the limits would be lower than for normal hydrogen. It is also assumed that water vapor and oxygen are equally effective as diluents. Upward flame-propagation for hydrogen in air can be produced down to a hydrogen concentration of 4.1% under normal conditions of temperature and pressure. It is estimated that the corresponding figure in oxygen would be 3.9% hydrogen. If we assume that the percentage reduction in lower limiting concentration with increasing temperature is the same for upward propagation as for downward, the lower explosive-limit would be 2.8% hydrogen at 300C. Increasing the pressure tends to increase the limit slightly, but we have neglected the correction in order to be conservative. Allowing a safety factor for possible unfavorable interactions of high temperature and high pressure, one would expect that the maximum allowable concentration of deuterium at conditions of 2000 psi and 300C would be about 2.0% by volume.

Recent work undertaken by Battelle Memorial Institute (7) is directed towards determining the explosive limits of hydrogen-oxygen in stoichiometric mixtures with water vapor as a diluent. These conditions would be encountered in a steam-pressurization system operated with 250C saturated steam. The early work carried out by Battelle in an autoclave 1.5 in. in diameter indicates that the lower explosive limit of hydrogen concentration in 250C saturated steam occurs when the partial pressure of stoichiometric mixtures of hydrogen and oxygen reached 200 psia. In similar experiments carried out in a 2.5-in. diameter autoclave explosions could be produced down to the point where partial pressure of stoichiometric mixtures of hydrogen and oxygen reached 90 psia in 250C saturated steam. With an even larger autoclave 3 in. in diameter, lower explosive limits had not been reached with certainty when the partial pressure of stoichiometric hydrogen-oxygen mixtures in 250C saturated steam was 10 psia. In the most recent Battelle report, it is significant to note that with 300C saturated steam as the diluent for hydrogen and oxygen in 1:1 ratio, explosions occurred in the 3.0-in. diameter autoclave with the hydrogen content as low as 0.37% by volume.

These results seem to indicate that the size of the container is a factor in the lower explosive limits in the hydrogen-oxygen water-vapor system. As it now stands, it has not been possible to firmly establish any absolute lower limit, and in fact, there is some concern whether this will be possible with the equipment presently being employed.

It is evident that an exact value for the explosion limit cannot be given at this time. A level of 1% deuterium was assumed for design as being the concentration that can be tolerated in the gaseous phase, and the operation of the pressurizer system was designed to maintain the deuterium concentration always below this level. This is a reasonable level, since it should be entirely possible to design the system to withstand an explosion at a concentration three or four times this value.

The pressures which can be developed by detonation and explosion have been calculated by the method used by Pigford. These calculations have been carried through to cover the particular conditions of pressure and temperature applicable to HRT for a D_2 concentration from 1 to 20% by volume. The results are presented in Figure 1.

C. PROCESS DESIGN FOR OXYGEN PRESSURIZATION

1. D_2 Concentration Control--Normal Operation

One of the first selections to be made in process design for oxygen pressurization concerns the method of explosive-mixture-concentration control. It should be possible to use either a process in which D_2 concentration in the pressurizer was held down by catalytic recombination or one in which venting served to carry away the D_2 . A thermosyphon type of catalytic recombiner could be installed in the pressurizers to prevent serious accumulation of deuterium. In normal operation, however, the amount of deuterium entering the pressurizer is so small that practically no recombination would be taking place most of the time. Under this circumstance, it would be difficult to determine whether or not the catalyst is retaining its activity. In addition, design of such a recombiner to accommodate the sudden influx of deuterium on the occasion of a "burp" would add considerably to the complexity of the pressurizer. In view of these difficulties, control of the deuterium content in the pressurizer by means of venting appears more reasonable.

As indicated by the flowsheets, the process recommended for the application of oxygen as a pressurizing gas is based on the principle of venting the pressurizers at a fixed, continuous rate and injecting oxygen at a rate

FIGURE I
DETONATION PRESSURES AS A FUNCTION
OF HYDROGEN CONCENTRATION

controlled to maintain the desired pressure. The fixed rate of venting has been selected to maintain the deuterium concentration at always below the assumed lower explosive limit of 1% deuterium during normal operation.

There should be no provision for the rapid release of excessive pressure in case the system pressure rises above the 2000-psi control point. Other control features already provided in the HRT system allow such release by the design of the letdown and dump valves which automatically open as relief valves if the pressure rises above 2800 psi. In addition, such an increase in pressure automatically throws the controls onto a dump cycle. In view of these provisions, we do not regard the addition of special relief devices on the pressurizers as necessary.

The practical problems of controlling the continuous vent are greatly simplified by the venting of the gas to a point of slightly reduced pressure in the high-pressure loop of the core circuit, instead of directly throttling the vent gas to the low-pressure system. This venting procedure sidesteps the need to add a valve or other flow-control device which must throttle the gas across a 2000-psi differential. With the proposed process, the flow of vent gas is throttled over a range of about 23 psi into the core loop downstream of the heat exchanger where it can be discharged to the low-pressure system by way of the present letdown valve, without adding to the severe conditions under which the valve operates.

In the design of this operation, it is necessary to first establish the quantity of radiolytic gas which can be expected to find its way into the pressurizer under conditions of normal operations, and in the event of a thermal surge. Detailed design calculations on the flow of gas under both normal operating conditions and on the occasion of a thermal surge, are presented in Appendix B.

Based upon experience in the HRE and subsequent mock-up tests, we were to assume that in normal operation, 1 cc/min of D_2 at reactor conditions will be continually entering the pressurizer. The pressurizing system is designed to hold the deuterium concentration always below the 1% hydrogen concentration explosive limit. Obviously, if gas is bled out of the pressurizer at a rate sufficient only to hold the composition in the pressurizer at 1% under steady-state condition, the operation would be in trouble if a "burp" caused the concentration to exceed 1% deuterium. Theoretically it would never again attain 1% concentration, but would instead be approaching this level asymptotically. Therefore, we decided to design the operation to hold the concentration at 0.75% hydrogen under steady-state conditions. Computations as presented in Appendix B were made to determine the bleed-out rate and therefore the oxygen-feed rate required to hold the

concentration of deuterium at this level. These calculations indicate that it will be necessary to introduce to each pressurizer 39 cc/min of oxygen at reactor conditions and bleed out 133 cc/min from each pressurizer of gas consisting of 70% heavy-water vapor, 29.25% oxygen, and 0.75% deuterium.

We determined the oxygen feed rate assuming no oxygen is absorbed by the liquid at the interface. The actual amount of oxygen required can best be determined by mock-up tests which could establish the absorption rate.

In addition to steady-state conditions, we must also consider a "burp" as it affects the concentration of deuterium in the pressurizer. It is considered that the greatest "burp" that could occur would be 10 l (i.e., 10 l of liquid rushing into the pressurizer in 0.1 seconds, carrying with it 1% by volume of gas composed of 20% deuterium, 10% oxygen, and 70% heavy-water vapor). If the volume of the pressurizer is 90 l, the 10-l addition would increase the deuterium concentration by 0.022%, if only the suspended gas is released. It should be possible, therefore, to take 10 "burps" in rapid succession without increasing the deuterium concentration from 0.75 to above 1.00%. It was also determined that this bleed-out rate would permit a "burp" as often as one every 15 minutes without exceeding the maximum deuterium content of 1% in the pressurizer. These calculations are also presented in detail in Appendix B.

The material and heat-balance calculation presented in Appendix C indicate the need to maintain isothermal conditions in the pressurizers.

2. D₂ Concentration Control--Emergency Dump

One must also consider the explosion hazards as they are affected by the gas-pressurization system in case of an emergency dump. Probably the only occasions which would call for an emergency dump would be a leak, a circulating-pump stoppage, or excessive pressure. In these cases the liquid must be removed and the pressure reduced as rapidly as possible.

The great danger in a dump operation arises from the possibility that boiling might occur during the discharge of fluid, with the result that dissolved and suspended deuterium and oxygen would be steam-stripped from the fluids and thus create a large accumulation of highly explosive mixture in the system. This problem of avoiding explosive mixtures during the dump operation has been given much study, and a number of possible methods have been explored. For example, we considered the possibility of carrying out the dump under full system-pressure by introducing steam

at 2000 psi from a steam accumulator. The steam would prevent boiling and reduce the possibility of explosive mixtures. This method has the disadvantage that the high-pressure-steam accumulator, although located outside the cell, increases the potential total pressure so that safe pressure in the cell may be exceeded in the event of equipment failure and consequent release of a large amount of energy into the cell from the accumulator. It also introduces a possibility of injury to the main heat-exchanger by thermal shock.

The ideal dump procedure would be one in which the solutions were dumped as rapidly as possible to minimize leakage of fissionable material to the cell, and in which the residual gases were disposed of quickly to minimize contamination of the cell with gaseous fission products. This procedure can be approximated with safety if the initiation of the dump is delayed just long enough for the catalyst to promote nearly complete recombination of the dissolved and entrained deuterium and oxygen in the core and blanket liquids. The power steam valve must be closed to stop energy withdrawal from the reactor. This causes the temperature to rise in the reactor until it approaches nominal power. At this elevated temperature, the deuterium-oxygen recombination rate is very rapid so that if the dump is delayed, the deuterium content of the system can be reduced to the extent that the core and blanket solutions can be released to the low-pressure system safely. Computations in Appendix D establish a dump procedure that will not allow dangerous concentrations of gaseous deuterium and oxygen mixtures to develop in the system. In these calculations it is assumed that the dissolved and suspended deuterium concentration of the blanket solution is the same as the core solution and that the recombination rate is the same in both solutions.

The initial calculations in Appendix D assume that isothermal conditions (i. e. , 280C) are maintained during the dump. The calculations were directed towards determining whether the total pressure of the system is reduced by catalytic recombination of the dissolved D_2 and O_2 so that boiling and, therefore, steam-stripping of the dissolved gases would not occur during the release of the liquids. These calculations indicate that if isothermal conditions can be maintained, and if the dump valves are delayed for one-half a minute, the pressure of the vapor space above the liquid will be always above the boiling pressure of the solution, and mass boiling will not occur, provided humidification of the vapor space from the surface of the liquid is complete. Mixing in both the liquid and vapor must be absolute during the dump. Since mixing will not be entirely complete, vaporization from the surface will cool the surface of the liquid and thus reduce the humidification rate to the extent that the pressure above the liquid will be reduced below the boiling point of the liquid under the cooled surface layer. When this occurs, mass boiling will take place, and the

surface of the liquid will be reheated and mixed by the boiling action. Thus some boiling would be expected, but the extent that boiling will occur is difficult to establish. The vapor released through mass boiling will be in equilibrium with the liquid from which it comes, and therefore the partial pressures of the vapors will be proportional to the vapor pressure of the constituents of the liquid. In order that development of explosive mixtures be prevented, the vapor pressure of the radiolytic gases must be reduced by catalytic recombination in solution to the extent that the composition of the vapor will be below the explosive limits. This avoids the concern over the possibility of mass boiling during the dump.

The calculations assuming isothermal conditions during the dump are greatly simplified as compared with the actual conditions. More rigorous calculations are presented in Appendix D for pump stoppage, taking into account the after-heat due to both nuclear fission and fission product heat. The temperature rise of the liquid due to this after-heat, assuming no heat removal or release from the system, has been calculated by J. Hill (8) for infinite reactor operation at 5 and 10 Mw and for one-day operation time at 5 Mw. As the solution temperature rises, its density decreases, causing a liquid expansion into the pressurizer. If the letdown valves are inoperative so that the liquid level is not controlled, expansion will cause adiabatic compression of the gas in the pressurizer thus increasing the total pressure of the system. When the pressure reaches 2800 psi the system would automatically dump.

The reduced neutron flux at increased temperature reduces the production of radiolytic gas, while increased temperature causes an increase in the catalytic recombination of D_2 and O_2 in solution as shown for 280°C and 300°C in Figure 2. This increase in the recombination rate reduces the dissolved radiolytic gas by an amount equivalent to the radiolytic gas in suspension in less than 5 seconds. In other words, in less than 5 seconds, the solution is unsaturated to the extent that it can absorb the suspended gas. This means that in the event of a pump stoppage, very little of the suspended gas will probably actually separate out. Taking into account the effect of increasing temperature of solution on the rate of recombination, a 1.88-minute delay time is required to reduce the radiolytic gas composition to the extent that the vapor pressure of D_2 is 1% of the total boiling pressure. Thus, if we initiate the dump after two minutes (even if boiling occurs), the vapor evolved will not exceed 1% D_2 . Actually a shorter delay of time may be employed since some degree of humidification can be expected, and the difference between gas pressure above the solution may be relatively large at the beginning of the dump if a one- to two-minute delay is employed.

FIGURE 2
CATALYTIC RECOMBINATION RATE D_2 & O_2 IN SOLUTION

Calculations were also made on the more likely dump procedure in which some heat would be removed. Rosenthal has estimated that in this situation the average temperature in the system would rise only slightly above 280 C. Pressure would not increase in the system since the liquid-level controls in the pressurizers would continue to discharge fluids to maintain the level of liquid. At 280 C, a 5-1/2-minute delay would be required to allow the D₂ and O₂ to recombine to the extent that if boiling occurs, the initial vapor will contain no more than 1% D₂. If the vapor space is humidified from the surface of the discharging liquid, this delay may be shortened, depending upon the rate of humidification. Since the humidification rate cannot be established, we must assume that upon the release of the liquids through the dump valves, the expansion of the vapor will be adiabatic. If the total volume of liquid is discharged in 10 minutes, it will take about 54 seconds before the vapor will have expanded such that the pressure will equal the boiling pressure of the solution. This means that the dump may be started 54 seconds earlier, or about 4-1/2 minutes after the neutron flux has been reduced. Since the degree of humidification that takes place during the liquid discharge can greatly affect the amount of delay required, it will be important to establish when boiling occurs in the initial dump trials on the reactor.

After the liquid has been discharged from the high-pressure circuits into their respective low-pressure system, the gas pressure throughout the high- and low-pressure systems must be reduced to atmospheric to prevent further leakage of radioactive material into the reactor cell. While most of the burden of pressure reduction is carried by the condensers in the low-pressure systems which serve to condense out the heavy water steam, about 154 cu ft of oxygen will remain for disposal. This amount of oxygen corresponds to the charge of oxygen required in the pressurizers in normal operation. As indicated on the flow diagram in Appendix F, we propose that this oxygen be disposed of by combination with ordinary hydrogen in a second low-pressure recombiner located just downstream of the heavy-water cold-trap.

3. Pressure Equalization

We considered several possible ways of equalizing the pressure in the two pressurizers. Under normal operating conditions, control of the oxygen-injection rates to the two pressurizers would be sufficiently sensitive to maintain the desired equalization of pressures. These controls could not be expected to maintain such equalization during abnormal conditions, however, so an additional means of equalization becomes necessary. Differential pressure-relief valves, located in an equalizer line joining the tops of the two

pressurizers, were considered for this purpose. Such valves would allow communication between the two pressurizers whenever the pressure-difference limits were exceeded. An analogous application of rupture discs was also considered. The complexities involved in applying either of these devices do not appear to be offset by any advantage over the use of an open equalizer line. Although an open line would permit free passage of vapor between the two pressurizers at all times (whereas, the differential relief valves or rupture discs would permit such passage of vapor only during abnormal conditions) there is no objection to intermixing of vapor between the two sides. Neither of these systems would prevent transfer of liquid from one pressurizer to the other in the event that one pressurizer became flooded with liquid, and its pulsifeeder pump were allowed to continue in operation. This cross-mixing of liquids would be highly objectionable and the need to prevent it places a heavy burden of reliability on the liquid-level control system. By the application of efficient entrainment separators in the top of each pressurizer, transfer of liquid from one pressurizer to the other should be prevented under all situations except failure of the level control system. The simplicity and excellent equalization of pressure attainable by the use of an open equalizer line justifies its selection, in preference to an equalizer line blocked with differential relief valves or rupture discs. Use of a large, open, equalizer line offers maximum assurance against overstressing the Zircaloy core vessel and the serious difficulties which would result from its rupture.

4. Second Recombiner

It is proposed that the gas-pressurization process include a second recombining both to facilitate disposal of oxygen and to make it possible to operate the charcoal fission-gas-adsorption beds with ordinary hydrogen as the carrier gas. This would be a catalytic recombining, designed for internal thermosyphon circulation through the catalyst bed, but operating always with a concentrated atmosphere of hydrogen instead of oxygen, as in the first recombining.

The unit would be designed for the emergency dump operation to recombine 154 cubic feet of oxygen STP per hour. Thus the unit would be greatly over-designed for normal operation.

Under normal operating conditions of the reactor, the oxygen-pressurizing-gas bleed would find its way from the present low-pressure system into the light-hydrogen recombining through a back-pressure-regulating valve. The valve is designed to maintain slightly higher positive pressure (1/2 psi) in the present low-pressure system, and possible contamination of

the heavy water with light hydrogen is avoided. The low-pressure recombiner would be maintained at slightly lower positive pressure (1/4 psi) by the introduction of hydrogen through a pressure-regulating valve. As oxygen is added and converted to water, the pressure in the light-hydrogen recombiner drops, due to condensation of the water vapor. When the pressure drops, more hydrogen is introduced through a control valve designed to maintain constant pressure in the recombiner at 1/4 psi. From the recombiner, hydrogen-carrying fission gases bleed off through cold traps to the charcoal beds at a constant rate. Since constant pressure is maintained in the recombiner by hydrogen introduction through a pressure-regulating valve the flow to the charcoal beds is maintained constant with a fixed capillary designed to permit flow of 1 cu ft per hour.

In the case of an emergency dump, the pressure-regulating valve is still operative, maintaining pressure in the low-pressure system, but a flow controller is also in the line from the present cold-trap system to the light-hydrogen recombiner. This flow controller serves to limit the flow of gas to 154 cu ft per hour. In this way the system provides automatic control at all times.

D. EQUIPMENT DESIGN FOR OXYGEN PRESSURIZATION

The basic engineering flow sheets of the HRT, D-18295, 18296, 18297 and 18298 have been modified to provide for the oxygen pressurization system. Prints of these modified flow sheets are in Appendix F. All equipment lines, valves, instruments, instrument-control lines are shown in accordance with the system employed in the original engineering flow sheets. The discussion which follows presents a description of the essential components of equipment and controls which should be added for oxygen pressurization.

1. Oxygen Injection System

As indicated on D-18296, the oxygen would be supplied in standard 220-cu-ft cylinders. These would be connected to a multistation high-pressure manifold from which the oxygen would be expanded to a controlled pressure level of 170 psi at the intake side of the oxygen compressor. A two-stage oxygen regulator such as that available from the Grove Regulator Company would be well suited for this purpose. Two Corblin model AICV single-stage oxygen compressors are recommended for compressing the oxygen from the 170-psi level to the 2500-psi level in the oxygen-accumulator tank. These compressors should be equipped with 3/4 hp motors and would be arranged in parallel so that one would serve as a stand-by.

The accumulator tank should have an internal volume of about 20 l to provide about two hours of operation without make-up from either compressor. The pressure in the tank would be held constant at 2500 psi by conventional adjustable, proportioning indicating controllers PIC (155 and 156) locally installed but equipped with high- and low-pressure alarm annunciators on the panel board. In effect, these control valves will serve as pressure-regulating by-pass valves around each compressor.

The flow of oxygen into each pressurizer is separately governed by a flow controller with an integral DP (differential pressure) cell. These controllers (FC 121, 221) are pneumatically reset by pressure controller (PC-150) installed on the pressurizer system. The control valve will be of needle-valve type. The feed lines to the pressurizers are equipped with check valves to protect against back-flow and with radiation monitors to detect any increase in activity occasioned by back-flow should it occur. These radiation elements (RE 164A, 164B, 264A, 274B) electrically actuate a control system which closes the line at FCV 220 and 120.

2. Pressurizer

The two pressurizer vessels would be identical except for the orientation of the pipe connections. Figure 3 shows the design layout of one vessel and Figure 4 shows a feasible installation layout. The vessel is of approximately 90 l internal volume and is equipped with flow rotation vanes at the bottom and an entrainment separator at the top. The rotation vanes are designed to develop a centrifugal action against the cylindrical wall of the vessel which prevents geysering of liquid up the center of the vessel. The entrainment separator is designed to handle wide variations in flow without appreciable pressure drop. Separated liquid drains back to the main body of the vessel. Oxygen is admitted by a 1/4 in. connection below the rotation vanes, and hot distilled heavy water is flowed onto the center of the rotation vane cone to allow for the humidification of the oxygen, which is dry on admission.

A large 3-1/2 in. pressure-equalizing line is recommended for tying the tops of the two pressurizers. While a small line would suffice for most situations, we believe that a large line is preferable because of the greater ability to equalize pressures in the event of an explosion in one of the pressurizers thus minimizing the possibility of rupturing the Zircaloy core vessel.

It is recommended that the pressurizers have thermostatically controlled electric heaters to hold them constantly at a temperature just below that of the minimum core temperature under normal operating conditions. As calculated in Appendix C, 2.5 kw heater capacity is required per pressurizer. Without this provision for "isothermal" operation, the partial pressure of water vapor will fall off in the pressurizers. This would necessitate an increased injection of oxygen to provide the same level of D₂ concentration. Also, very rapid humidification of dry gas by a "burp" would result in an additional pressure rise of considerable size.

3. Bleed-out System

Gas will be bled off from both pressurizer systems through a common bleed-down line. This line originates at the equalizer line and will discharge to the high-pressure core system downstream of the heat exchanger. This bleed-down line will be provided with a fixed capillary 0.03 in. diameter and 3.7 ft long. The capillary will be protected from plugging by the incorporation of a large, porous, stainless steel filter upstream to prevent any solid particles from entering. A whistle downstream of the orifice will indicate the flow. An electronic pickup will be installed outside the pipe at this point, and the sound will be amplified to a speaker mounted on the main control panel. It is advisable to discharge the bleed from the pressurizers to the high-pressure loop downstream of the heat exchangers, rather than

SECTION A A

ORIENTATION

FIGURE - 3

MATERIAL: 307 ST. STEEL

FIG. 3
PRESSURIZING TANKS

directly to the low-pressure system. In this manner, a reasonable pressure drop and therefore a reasonable size orifice for control of flow can be obtained. Such a system avoids the need to use additional control valves and makes the orifice large enough to be practical.

4. Dump Controls

A few modifications in the present dump cycle are recommended if an oxygen pressurization system is to be used. There should be no rapid venting from the tops of the pressurizers. This procedure would tend to bring deuterium-rich vapor into the pressurizer from the core and blanket solutions. The fixed capillary bleed-down would continue to pass some flow during parts of the dump cycle, but this flow is so small as to be insignificant for the short time interval involved. Of primary importance are the immediate closing of the power steam-stop valve, and enforcement of a four-minute-50-second period for the recombination of D_2 and O_2 within the liquids. Next should come steps which would permit the pressure in the high-pressure system to drop below 2000 psi. At the end of the delay period, both the core and blanket dump valves should be opened wide simultaneously.

The dump systems should have built-in flow characteristics to proportion the ratio of discharge from the core and blanket so that they empty nearly simultaneously. If one can make certain that neither system will have much liquid remaining in it when the other goes dry, any tendency toward severe boiling (and possible cross-mixing by carry-over through the pressurizers) will be eliminated. We recommend that the discharge rates be proportioned by the careful sizing of the piping and dump valves so that the major portion of the resistance to flow at dump velocities occurs in the piping downstream of the dump valves. If necessary, special restrictor sections could be welded into the downstream sections to match the systems.

5. Second Recombiner Equipment

The arrangement of the second recombiner and its controls is shown on engineering flow-sheet D-18295. Design layout of the recombiner itself is given on Figure 5. This design is based on calculations presented in Appendix E. Under all anticipated conditions of operation, the gas fed to this recombiner from the present heavy-water cold-trap will be dry oxygen of over 99% purity in which the fission gases are carried. Rather than go to the added complexity of using light-water steam at this point as a diluent, it is proposed to feed this concentrated oxygen directly into the catalyst bed and provide a very high rate of recirculation of the H_2 in the recombiner to

SECTION B-B

PLAN

SECTION A-A

REFERENCE DWG.
COOLING WATER LINES TO THERMAL & BLAST SHIELDS
OAK RIDGE DWG. NO. E-24711

FIGURE - 4

FIG. 4
PRESSURIZER LAYOUT

FIG. 5
 RECOMBINER

serve both as the reactant and the diluent. The proposed design is based on the acceptance of a maximum feed rate of 154 SCF per hour. At all times a H_2 recirculation rate of at least 7720 SCF per hour is to be maintained by the thermal convection action of the built-in heating and cooling coils. Thus, the extent of dilution within the catalyst bed is such that the average temperature rise at maximum feed rate is $286^\circ C$. With the platinized stainless steel gage catalyst mounted as indicated, essentially complete recombination of all oxygen fed to the recombiner is expected. The residual gas passed on to the charcoal fission-gas adsorbers would be over 99% H_2 carrier.

The H_2 injection system would be supplied with compressed hydrogen from standard 220 cu ft commercial gas cylinders connected to a multistation high-pressure manifold serving the H_2 feed line through a H_2 pressure-regulating valve set for 25 psi. A second pressure-regulating valve "PCV757" would inject H_2 to the second recombiner at whatever rate was needed to maintain its pressure at 1/4 PSI.

The control of feed from the low-pressure system of the core and blanket to the second recombiner requires more than one control method because of the wide range of flows which must be handled i.e., normal operation versus dump operation. Rapid processing of gas following an emergency dump involves a 30-fold increase in flow over that for normal conditions. Flow control valve FCV-730 is designed to limit the maximum flow rate to the design value of 154 SCF per hour. For conditions requiring less than maximum flow, this valve remains full open, and the back pressure regulating valve PCV-750 comes into action. In response to decline of pressure in the low-pressure system, it slowly gives increased throttling action until flow practically ceases at 1/2 PSI in the low-pressure system i.e., line 117 in Figure D.

As a safeguard against backflow of H_2 into the low-pressure systems of the core or blanket where it would become mixed with D_2 , a check valve is indicated. In addition, a pressure-differential element PDE 756 is tied into the system so that when the pressure in the recombiner approaches that in the low-pressure system, stop valve PCV 756 will be seated tightly. A further precaution is recommended in the form of high- and low-temperature sensing elements and an alarm annunciator to call attention to any failure of the built-in condenser or steam heater to function, as by stoppage of cooling water. Such a failure would of course tend to result in a pressure rise in the recombiner which would set up a potential for back flow after further reduction of pressure in the low-pressure system. This alarm can also warn of a condition which could prevent the H_2 feed system from functioning.

Cold traps in duplex, identical with those in the present low-pressure systems, are proposed for the reduction of the dew point of the gas passing to the charcoal beds. All light water collected from the recombiner condenser and the cold traps would be directed to present hold tanks for light water.

Control of flow to the charcoal fission-gas-adsorption beds becomes very simple because the second recombiner is held at constant pressure. We recommend a fixed orifice designed to give 1 SCF per hour at 1/4 psi pressure drop, and base this recommendation on the assumption that pressure drops through the cold trap and charcoal bed are negligible.

Because the proposed design of the second recombiner involves considerable departure from the designs currently in use, we urge that full scale mock-up trials of the design be conducted before it is adopted.

E. DESIGN FOR CONFINEMENT OF EXPLOSIONS

The inevitable presence of free deuterium and oxygen gas is of constant concern in the design of the aqueous homogeneous reactor. While it is possible to contrive process cycles which positively avoid accumulation of hazardous quantities of deuterium in normal operation, one can never be sure that accidental or inadvertent departure from normal procedures will not lead to conditions under which a severe explosion can occur. Pigford points out that the ratio of pressure increase under worst conditions may be as high as 14-fold and is likely to be at least eight-fold. The designer is thus left little choice but to provide some means to prevent catastrophic spread of radioactive material if the pressure-confining equipment should rupture. The gas-tight cell surrounding the hazardous portions of the HRT equipment is designed to confine the entire product of any possible explosion if pressure on the walls of the cell does not exceed 30 psi.

Although use of this cell surrounding the hazardous equipment should provide adequate safety for all personnel, the cell in itself gives no assurance that the reactor will have the continuity of operation necessary for successful commercial power production. A rupture of the high-pressure equipment resulting in severe contamination of the cell would require so long to repair that this type of failure must be extremely infrequent if the system is to be commercially acceptable. This consideration makes examination of all possible means of confining explosions within the process equipment a subject of pertinent interest.

We have calculated the static and impact pressures resulting from explosion and detonation in a mixture of oxygen and hydrogen initially at 2000 psia and 300C. It is assumed that the partial pressure of water in the

reactant mixture is 1250 psia, and that there will be no dissociation of the reaction products. Allowing for dissociation would give calculated pressures somewhat lower than those shown in Figure 1.

The core and blanket liquids, within their respective high-pressure circulating systems, will be saturated with dissolved deuterium and oxygen in stoichiometric ratio as well as free gas normally entrained as bubbles in the circulating liquids. The peak pressure indicated for detonation of these bubbles is about 34,000 psi. Thus, any situation which leads to a chain detonation of a large percentage of these bubbles, or detonation of a single large bubble, will certainly lead to pressures too high to be confined by anything other than extremely special equipment. Such a situation could result if an explosion occurred in the pressurizer, and the detonation wave transmitted through the liquid created auto-ignition temperatures in the bubbles. Another possible dangerous situation could arise if a bubble of sufficient size developed in the loop so that ignition could occur as the result of a self-accelerating chemical reaction between D_2 and O_2 . Existing data on H_2 and O_2 shows that at the temperature and pressure of the system, gas bubbles 10 inches or more in diameter would ignite in this way. Explosion would be preceded by an induction period. The length of such an induction period is a complex function of several factors and can not generally be predicted.

If detonation is avoided in an explosion in the loop, the peak pressures would be greatly reduced, and the rate of combustion might be such that heat transfer to the liquid would have an effect in reducing the pressures even below those indicated from the static pressure curve presented in Figure 1. For the 20% D_2 concentration resulting from gas evolution within the high-pressure liquid circuits, a static pressure of 15,000 psi would be expected from explosion. This pressure would build up so rapidly that no conventional pressure-release means such as safety valves or rupture discs could be applied to give the desired protection. Because of inertial effects, it would be necessary to locate such relief devices at many points in the system. The necessity of discharging such devices into closed systems and, in the case of bursting discs, the need to open up the system in order to replace discs, create serious practical problems.

No really practical mechanical design is apparent for the solution of this problem. A logical approach would be to provide special means for energy absorption on the internal surfaces on all the pressure-confining walls. Such a means might consist of an inner pressure-tight corrugated-metal liner of just sufficient stiffness to resist plastic deformation at normal operating pressures. Between this and the heavy outer wall would be a granular material selected on the basis of its ability to absorb energy as a result of deformation. It is believed that such a system could be developed to withstand explosions and also to act as a damper against build-up of shock waves.

There are obvious severe limitations for practical use, however, because it would be impossible to absorb the energy from more than just a few explosions in any one section of the system before the equipment had to be disassembled and the energy-absorbing means reworked.

The problem of designing for the confinement of explosion appears to be far more difficult for those parts of the high-pressure system in which the core and blanket liquids normally circulate than it is for the pressurizers themselves. It is reasonably straightforward to provide assurance that the concentration of deuterium in the gas space of the pressurizers will always be held at a low value. Basic control can be achieved by the use of appropriate rates of injection of the pressurizing gas and corresponding rates of venting, and it is planned to hold D_2 content of the pressurizers always below 1% in the proposed oxygen-pressurization system. In addition, it is possible to monitor the pressurizers with explosive-mixture sensing devices so that the effectiveness of the injection and venting system can be continuously verified. The highest concentration of deuterium which can occur within the pressurizers under any reasonable situation is about 3-1/4%. This high concentration can develop in the blanket pressurizer if the blanket circulating-pump fails and the suspended gas rises into the pressurizer. The peak pressure for detonation at this concentration is 9500 psi. Since the impact pressure is of extremely short duration, we recommend that the pressurizer be designed for the static pressure or 6500 psi. The inherent safety factor in this design will allow for the short-duration impact-pressure. Thus one would expect at most only slight elastic deformation of the vessel. On the other hand, there seems to be no ready way to provide comparable assurance that malfunction or misoperation within the circulating system will not lead to explosion pressures of 15,000 psi or more. These would be very difficult to confine.

F. CHARCOAL FISSION-GAS-ADSORPTION BEDS

A study of gas-pressurization systems would be incomplete without a study of fission-gas disposal, since many of the conceivable gas-pressurization schemes involve increased rates of gas-venting which, in turn, may impose an increased burden on the fission-gas-disposal system. The disposal means used in HRE and planned for HRT provides a long decay time of the fission gases on activated charcoal. Each adsorber bed contains 14 cu ft of activated charcoal packed in water-cooled pipes which vary in size from 1/2 in. at the inlet to 6 in. at the outlet. The system is designed with gradually increasing diameter of piping to match cooling characteristics of the bed against the fall in rate of heat evolution with increase in residence time of the adsorbed radioactive gases. The actual volume of the fission gases is so minute that the system is designed on the basis of oxygen being used as a carrier gas to reduce the rate of energy released per unit of volume and

facilitate heat transfer to the pipe walls. The general philosophy behind this design is to provide a system which gives high assurance of a long residence-time to allow almost complete radioactive decay. Pressure is held at atmospheric, and all parts of the system are simple and rugged to minimize the likelihood of any rapid increase in the radioactive content of the gas vented to the stack, as a consequence of accidental or inadvertent departure from normal operating procedures.

We believe, however, that use of oxygen as the carrier gas is inconsistent with this general philosophy. In effect, the voids of the bed will be filled with practically pure oxygen at one atmosphere, and considerable amounts of oxygen will be adsorbed on the charcoal. This presents the possibility of uncontrolled combustion if, for any reason, ignition should occur. While there is insufficient oxygen present to generate a violent explosion, there is enough to support a rise in temperature in the bed of about 85C. Such a rise in temperature would be expected to drive off a large fraction of the adsorbed gases in a short time, which is exactly what the bed is intended to prevent. Consequently, we recommend that ordinary hydrogen be used as the carrier gas.

The use of ordinary hydrogen can increase the capacity of the charcoal beds, as well as eliminate the possibility of combustion within the bed. Fundamentally, the design of a charcoal adsorber-bed for this service is dictated largely by heat transfer considerations. The very high thermal conductivity and heat capacity of hydrogen, as compared with oxygen, would improve the heat-transfer rates several-fold, resulting in a corresponding increase in the ability of the bed to maintain a long residence time.

A specific question has been raised concerning the ability of the charcoal beds to function in the presence of water vapor. According to the design of the HRT, the carrier gas entering the charcoal bed should have a dewpoint close to -20C because all gas must pass through a cold trap at this temperature before entering the bed. Frosting of the heat exchange surfaces in the traps, however, or imperfect operation of the refrigeration system can lead to a situation under which gas of a much higher dewpoint might enter the beds. Since the adsorption characteristics of charcoal are readily modified by exposure to various materials, including water vapor, one cannot predict the exact behavior of a bed without detailed specific knowledge of the particular charcoal used in the bed and the entire history of its operation, subsequent to initial activation. In some charcoals, and particularly the ones which have silica as an impurity, the adsorption-desorption process has a pronounced hysteresis which makes it very difficult to drive the water from the bed, once it has been adsorbed. In an activated-charcoal bed which is in good condition, the following considerations govern the dewpoint that can be tolerated in the influent gas.

1. The adsorption of a gas or an adsorbent will not be appreciably affected by the presence of another gas if only a small fraction of the surface of the adsorbent is covered with gas molecules.

2. The area of activated adsorbent charcoals is 500-800 sq m/gm. In order to have this surface covered with an unimolecular layer of water molecules, 130.5-209 mg of H_2O would be required per gm of charcoal. (Area of H_2O molecules is assumed 11.5×10^{-16} sq cm.) Such a condition would undoubtedly exhaust most of the Xe and Kr.

3. If the surface were only 25 per cent covered with H_2O molecules, the effect on the adsorption of Xe and Kr would be small and tolerable although not advisable.

4. The amount of H_2O absorbed on charcoal depends on the ratio of partial pressure of H_2O in gas to vapor pressure of water at temperature of adsorbent P_v . A ratio of partial pressure of H_2O in gas to vapor pressure of water of 0.5 is required to achieve a complete layer of water molecules; a ratio of about 0.2 is required to achieve 25 per cent coverage. Therefore, the dew point required in entrant gas varies depending on temperature of the adsorbent.

5. The following summarizes the dew points required in the gas to prevent desorption of Xe and Kr for different charcoal-bed temperatures:

<u>H_2O DEW POINTS</u> (°C)			
<u>Charcoal Bed Temperature</u>	<u>Recommended</u>	<u>Tolerable 25-35% of Surface Covered with H_2O</u>	<u>Intolerable 100% of Surface Covered with H_2O</u>
0	-40	-18.2	-8.2
15	-30	-6.8	4.7
30	-20	4.6	18.5

The data of the table shows that a warm charcoal bed has the advantage of allowing a high dew point in the entrant gas. During winter weather if charcoal absorber gets quite cold, a low dew point is necessary. During summer, or if the heat from the radioactivity maintains the temperature high, then a much higher dew point would be allowable.

APPENDIX A

CONSIDERATION OF INDIRECT GAS-PRESSURIZATION METHODS

In many ways, the most satisfactory gas-pressurization system would be one which provides a flexible partition between the pressurizing gas and the core and blanket liquid; thus the problem of the pressurizing gas contaminating the fluids and the hazard associated with radiolytic gases entering the pressurizer are avoided. Theoretically, this ideal solution could be accomplished in several ways, and some of the more obvious were considered as discussed below.

A. MECHANICAL METHODS

A large bellows might serve as a flexible partition between the liquid and the pressurizing gas. It would be necessary to attach the bellows in such a way that neither gas bubbles rising nor slurry particles settling would accumulate in the bellows system. This might be done by mounting the bellows on a horizontal axis, with the liquids flowing up or down in the external convolutions.

Development of a satisfactory bellows for such use would involve very close attention to corrosion and fatigue problems. It would also be necessary to integrate the present liquid-level-sensing element into the bellows system, probably by using bellows position as the primary indication of level. Additional development problems would be encountered in large-scale-reactor application.

It is also conceivable that a piston could be used as a movable partition between the liquid and the pressurizing gas. The piston would require lubrication, but this could be accomplished if heavy water or feed solution were force-leaked by the piston into the high-pressure loop. This type of equipment suffers from the same disadvantages as the bellows as regards level control and the problems of avoiding pockets which might accumulate radiolytic gas or slurry particles. Design of such a device for large-scale reactors would also involve problems.

B. LIQUID SEALS

A flexible partition between pressurizer and liquid can also be accomplished through the application of an immiscible liquid leg. Such liquids as mercury, low-melting bismuth-lead alloy and molten sulfur were considered only briefly because of chemical incompatibility with the reactor solutions. One

practical approach to the flexible liquid partition can be accomplished, however, through the introduction of the 1 gpm feed into the pressurizer legs, thus providing a positive flow to overcome back diffusion of deuterium and oxygen from the high-pressure loop into the pressurizer. Detailed calculations on this arrangement are presented below:

The following calculations were made to establish the effectiveness of introducing the feed (1 gpm) into the 3-in. lines to the pressurizers to prevent migration of deuterium into the pressurizer by diffusion.

The flow velocity of solution through the pipe is:

(Assuming a 3-in. ID pipe)

$$\frac{1 \times 231}{7.07} = 32.7 \text{ in. /min}$$

or

$$\frac{32.7 \times 2.54}{60} = 1.38 \text{ cm/sec}$$

The above velocity is for plug flow, which has been assumed for these calculations.

The distance that the hydrogen (deuterium) will diffuse into the pipe, again assuming plug flow, is calculated as follows:

$$N_a = U_a C_a = -D \frac{(C_a + C_b)}{C_b} \frac{dC_a}{dx} \quad (\text{Sherwood}) \quad (1)$$

where N_a = diffusion rate gm moles/sec sq cm

U = velocity of diffusing component cm/sec

D = diffusivity sq cm/sec

x = distance in direction of diffusion, cm

C_a = partial molal density, gm moles/cc (D_2)

C_b = partial molal density, gm moles/cc (D_2O)

rearranging:

$$U_a dx = -D \left[\frac{1}{C_b} + \frac{1}{C_a} \right] dC_a$$

Assuming C_b is constant, and integrating over a length of pipe x cm long, through which the hydrogen (deuterium) concentration varies from that in the circulating loop to zero concentration, which is assumed for the incoming stream, we can develop:

$$U_a \int_0^x dx = D \frac{1}{C_b} \int_{C_{a1}}^0 - D \int_{C_{a1}}^0 \frac{dC_a}{C_a}$$

which upon integration yields:

$$U_a = \frac{D}{x} \left[\frac{C_{a1}}{C_b} + C_{a1} \right] \quad (2)$$

Determination of D at 2000 psia and 300 C for hydrogen which will be of the same order of magnitude as for deuterium:

$$D = 5.13 \times 10^{-5} \text{ sq cm/sec for H}_2 \text{ (Chemical Engineers Handbook, Perry)}$$

From the Theory of Rate Processing, Glasstone, Laidler, and Eyring:

$$\frac{Du}{T} \text{ is independent of temperature}$$

where u = viscosity

T = absolute temperature

This relationship is assumed to be independent of pressure which, for liquids, should not introduce any appreciable error.

	<u>20 C, 14.7 psia</u>	<u>300 C, 2000 psia</u>
T °K	293	523
μ centipoises	1.005	0.093

$$D = 5.13 \times 10^{-5} \times \frac{1.005}{0.093} \times \frac{523}{293} = 1.085 \times 10^{-3} \text{ at 300 C and 2000 psia}$$

C_{a1} , the dissolved hydrogen concentration in the reactor solution, is found by using Henry's Law, as the solution will be saturated with respect to hydrogen.

$$k = \frac{P_a}{X_a}$$

$$k = 2.0 \times 10^5 \text{ for } H_2 \text{ at 300 C}$$

$$P_a = 754 \times 0.667 = 502 \text{ psia}$$

$$\text{therefore } X_a = \frac{502}{2.0 \times 10^5} = 2.51 \times 10^{-3} \text{ gm moles } H_2 / \text{gm mole } H_2O$$

$$\frac{1 \times 20}{0.73} = 27.4 \text{ cc/gm mole } D_2O$$

$$C_{a1} = \frac{.00251}{27.4} = 0.0000915 \text{ gm moles } D_2 / \text{cc}$$

C_b is the D_2O concentration in the reactor solution at 300 C and 2000 psia in gm moles D_2O /cc:

$$\frac{1}{27.4} = 0.0365 \text{ gm moles } D_2O / \text{cc}$$

We now calculate the distance up the pipe which the dissolved D_2 will diffuse, setting U_a equal to the velocity of the solution flowing countercurrent to the diffusing D_2 .

$$x = \frac{1.085 \times 10^{-3}}{1.38} \left[\frac{0.0000915}{0.0365} + 0.0000915 \right]$$

$$x = .0073 \text{ cm}$$

This is the distance D_2 would diffuse up the pipe, assuming plug flow. Plug flow would not be attained, however, and a layer of stagnant solution near the pipe walls would be encountered. As diffusion of D_2 through this stagnant layer occurred, however, the deuterium would tend to diffuse toward the center of the pipe, and would therefore be carried out with this faster-moving liquid.

APPENDIX B

CALCULATIONS OF THE REQUIRED FLOW OF PRESSURIZING GAS TO AND FROM THE PRESSURIZERS

A. CALCULATION OF DEUTERIUM FLOW INTO THE PRESSURIZER

In the original statement of the problem it was indicated that 5 cc/min of gas (D_2 , O_2 , and D_2O) at reactor conditions will enter the pressurizer (core side) during normal operation.

$$D_2 = 0.3 \times 2/3 \times 5 = 1.0 \text{ cc } D_2/\text{min}$$

$$O_2 = 0.3 \times 1/3 \times 5 = 0.5 \text{ cc } O_2/\text{min}$$

$$D_2O = 0.7 \times 5 = 3.5 \text{ cc } D_2O/\text{min}$$

If the leg to the pressurizer, however, contains essentially stagnant solution, then diffusion of gases should be controlling. The following calculations were made to determine the amount of gas which can enter the pressurizer by diffusion.

Using the equation:

$$N_a = D \frac{(C_a + C_b)}{C_b} \frac{C_{a1} - C_{a2}}{X} \quad (\text{Sherwood}) (1)$$

where N_a = diffusion rate of D_2 in gm moles/sec sq cm

D = diffusion coefficient sq cm/sec

C = partial molal density gm moles/cc

X = distance in direction of diffusion cm

Subscript a refers to D_2 , b refers to D_2O , and 1 and 2 the higher and lower concentrations respectively.

In making the following calculation we have assumed that the D_2 must diffuse through a 6-ft length of pipe containing stagnant D_2O . The 6-ft length is believed to be reasonable in that convection currents have been non-existent for this distance in the passenger leg.

$$\begin{aligned}
 D &= 1.085 \times 10^{-3} \text{ sq cm/sec} \\
 C_{a_1} &= 9.15 \times 10^{-5} \text{ gm moles/cc} \\
 C_b &= 0.0365 \text{ gm moles/cc}
 \end{aligned}$$

The above values were calculated in Appendix A.

C_{a_2} is assumed to be zero, the worst condition.

C_a is taken as the average D_2 concentration.

$$C_a = 1/2 (0.0000915 + 0) = 0.0000457 \text{ gm moles/cc}$$

$$N_a = 1.085 \times 10^{-3} \left(\frac{0.0000457 + 0.0365}{0.0365} \right) \frac{0.0000915 - 0}{183}$$

$$N_a = 5.44 \times 10^{-10} \text{ gm moles } D_2/\text{sec sq cm}$$

If we assume a 3 in (7.62 cm) ID equivalent of 45.6 sq cm cross-sectional area, the volume of D_2 entering the pressurizer per minute by diffusion is:

$$5.44 \times 10^{-10} \times 60 \times 45.6 \times 22,400 \times \frac{14.7}{2000} \times \frac{573}{273} = 5.14 \times 10^{-4} \text{ cc}$$

It is apparent that the major portion enters the pressurizer by means other than diffusion.

B. CALCULATIONS OF CONTINUOUS-BLEED-RATE REQUIREMENTS

If gas pressurization is used, it will be necessary to employ a continuous bleed-out so that build-up of the D_2 concentration in the pressurizer can be prevented. While one can conceive of various means of minimizing the transfer of D_2 to the pressurizer-gas space (such as applying the feed to the pressurizer leg), none of the methods considered are completely reliable under all conditions ("burp," pump failure, etc.).

The following calculations establish the oxygen-heavy water vapor-bleed-out rate required to maintain the D_2 concentration below 1% in the pressurizer. Recent work on explosive limits has indicated that explosions

under the conditions within the pressurizer can occur down to or possibly below 1% D₂. Since the actual limit has not been found, we have assumed 1% to be the limit. Therefore, the D₂ concentration must be maintained at or below this value.

It is assumed that deuterium enters each pressurizer at 1.0 cc/min, as discussed above. If gas pressurization is used, however, an additional source of D₂ might arise due to humidification of the incoming O₂ from the solutions in the legs to the pressurizers with the resulting liberation of the D₂ associated with the D₂O vaporized. Humidification does not necessarily have to be accomplished using fuel solutions, so this D₂ is not included in the first approximation.

If the D₂ is to be held at 1%, the bleed-out rate must be 100 cc/min, of which 70 cc would be D₂O and 30 cc would be D₂ and O₂ (1cc D₂ and 29 cc O₂). Therefore, the oxygen bleed-in rate would be 29 cc per minute at reactor conditions, assuming no oxygen were absorbed by the solution. If we convert to standard temperature and pressure, the oxygen flow rate required is:

$$\frac{29}{14.7} \times \frac{2000}{573} = 1880 \text{ cc/min STP}$$

If a thermal surge or "burp" occurs, the rate of entrance of D₂ into the pressurizer is increased. A surge of 10 l of liquid is considered the maximum "burp" to be expected. One 10-l "burp" would produce (if the liquid comes directly from the core) 100 cc of gas, consisting of 70 cc D₂O, 10 cc O₂, and 20 cc D₂ at reactor conditions, the result is:

$$\frac{20 \times 100}{90,000 + 100} = 0.022\% \text{ increase in the D}_2 \text{ concentration.}$$

The above increase assumes that only the suspended gas is liberated in the pressurizer. This, however, will not be a reality, as some of the dissolved gas will be stripped from the solution. Counteracting the stripping, is the fact that a considerable portion of the liquid entering the pressurizer is the stagnant solution held in the pressurizer leg. We believe, therefore, that the 0.022% increase is a conservative figure, especially when one realizes that catalytic recombination of D₂ and O₂ is continually taking place in the leg.

As calculated above, if 29 cc of O_2 is added and 100 cc of mixture is bled out continuously, the system is balanced at 1% D_2 concentration (the D_2 addition is 1 cc per min and the O_2 addition is 29 cc/min). When a 10-1 "burp" occurs, however, the D_2 concentration increases from 1% to 1.022%. Since the over-all gas normally entering the pressurizer is 1% D_2 , an infinite time will be required to reduce the concentration from 1.022% to 1% D_2 . Moreover, the 1% limit set for D_2 concentration is exceeded by a single 10-1 "burp."

Suppose, therefore, we hold the D_2 concentration under normal operation below the explosion limit at 0.75%, leaving a margin of safety, we would then have to bleed out

$$\frac{1}{.0075} = 1.33.3 \text{ cc/min reactor conditions}$$

This means that 39 cc at reactor conditions, or 2530 cc of oxygen at standard temperature and pressure per minute, must be added to each pressurizer.

We must now calculate, at this bleed-out, the rate at which the concentration of D_2 will drop from the limiting concentration of 1%. In other words, suppose the concentration somehow attains the limit of 1% D_2 . How fast is this concentration reduced by the 133-cc bleed-out?

$$\text{Input-Output} = \text{Accumulation}$$

$$\text{Input} = 133 \times 0.0075 \, d\theta \text{ as } D_2 \text{ cc/min at reactor condition}$$

$$\theta = \text{time (minutes)}$$

$$\text{Output} = 133 \, c \, d\theta$$

$$c = \text{concentration } D_2 \text{ in bleed stream at any time.}$$

$$\text{Accumulation} = Vdc$$

$$V = \text{volume of system (90,000 cc)}$$

$$133 \times 0.0075 \, d\theta - 133 \, c \, d\theta = 90,000 \, dc \quad (1)$$

or

$$d\theta = \frac{90,000}{133} \left(\frac{dc}{(0.0075 - C)} \right)$$

solving for θ

$$\theta = + \frac{90,000}{133} \ln \frac{C_1 - 0.0075}{C_2 - 0.0075} \quad (2)$$

C = concentration D_2 in exit gas at any time θ

C_1 = highest allowable D_2 concentration 1% at θ_1

C_2 = concentration just before next "burp" at θ_2

r = cc/min bleed-out rate = 133 cc/min

$$\text{Elimination Rate} = \int_{\theta_1}^{\theta_2} r c d\theta = D_2 \text{ per "burp" or 20 cc reactor condition.} \quad (3)$$

From equation (2)

$$\theta = \frac{v}{r} \ln \frac{0.01 - 0.0075}{c - 0.0075}$$

$$\frac{\theta r}{v} = \ln \frac{0.0025}{C - 0.0075}$$

solving for C:

$$c = 0.0075 + 0.0025 e^{-\frac{r\theta}{v}} \quad (4)$$

Substituting in equation (3):

$$20 = \int_{\theta_1}^{\theta_2} r \left(0.0025 e^{-\frac{r\theta}{v}} + 0.0075 \right) d\theta$$

Taking θ_1 as zero time and integrating:

$$-0.0025 \frac{v}{r} \left[e^{-\frac{r\theta_2}{v}} - 1 \right] + 0.0075 \theta_2 = \frac{20}{r}$$

By trial and error $\theta_2 = 15$ min.

This indicates that operating with a bleed-out rate of 133 cc gas/min at reactor conditions, the system can tolerate one 10-l "burp" every 15 minutes.

Thus the operation of the pressurizer will involve bleeding out $133 \times 0.3 = 40$ cc of $D_2 + O_2$ per min at reactor conditions. The D_2 discharge will be 1 cc per min, therefore the maximum O_2 required will be 39 cc per min assuming no oxygen absorption in the reaction solution.

If humidification is accomplished by the vaporization of the solution in the leg, then a certain amount of D_2 and O_2 will be carried into the pressurizer with the water vapor.

D_2O that must be vaporized per minute is $133 - 39 = 94$ cc at reactor conditions, or 133 cc D_2O vapor at 1250 psia and 300C.

$$V_g = 0.3450 \text{ cu ft/lb} \quad (\text{Steam Tables})$$

$$\frac{1}{0.3450} \times 0.01602 \times 133 \times \frac{1}{0.73} = 8.47 \text{ cc/min } D_2O \text{ for humidification.}$$

The dissolved D_2 associated with this amount of liquid, if humidification takes place from the liquid interface, is:

Concentration = 2.51×10^{-3} gram moles D_2 per gram mole of solution

$$\frac{8.47 \times 0.73}{20} \times 0.00251 \times 22,400 \times \frac{14.7}{2000} \times \frac{573}{273} = 0.268 \text{ cc } D_2/\text{min and}$$

approx. 0.134 cc O_2 /min

This amount is based on the assumption no recombination takes place in the leg and the amount is found to be small enough so that it is not believed necessary to recalculate the required bleed-out rate to compensate for this additional D_2 . To insure the necessary vapor we consider it advisable to introduce D_2O distillate to each pressurizer. Moreover, it is difficult to firmly establish whether the humidification could be accomplished from the liquid surface in the pressurizer line.

APPENDIX CMATERIAL AND HEAT BALANCE CALCULATIONSA. MATERIAL BALANCE AROUND PRESSURIZER

<u>Material In</u>	Volumes at 2000 psia and 300 C
Oxygen	39 cc/min (O ₂ bleed in)
D ₂ O	4.31 gm/min (condensate addition)
D ₂	1 cc/min (R. B. Briggs estimate)
<u>Material Out</u>	
	133 cc/min as the purge stream of the following composition:
	D ₂ O 70%
	O ₂ 29.25%
	D ₂ 0.75%

This material balance is for steady-state operation, assuming no "burps" and assuming 100% humidification of the entering oxygen. The condensate addition is taken as that required for humidification.

B. HEAT BALANCE AROUND PRESSURIZER

<u>Heat Out</u>	<u>Datum Temp. 18 C</u>
Purge Stream	300 C
D ₂ O	$4.31 \times .002205 \times 1181.0 = 11.22 \text{ Btu/min}$
O ₂	$39 \times \frac{2000}{14.7} \times \frac{372}{572} \times \frac{1}{22,400} = 0.1125 \text{ gm moles/min}$

Heat OutDatum Temp. 18 C

$$O_2 \quad 0.1125 \times (300 - 18) \times (7.28 - 0.5) \times \frac{1}{252} = 0.855 \text{ Btu/min}$$

$$D_2 \quad 0.00288 \times (300 - 18) \times 6.99 \times \frac{1}{252} = 0.0225 \text{ Btu/min}$$

$$\text{Total} \quad 11.22 + 0.855 + 0.023 = 12.098 \text{ or } 726 \text{ Btu/hr}$$

Heat Loss

Oak Ridge has measured over-all U at 1.45 Btu/hr/sq ft/° F when the heat transfer coefficient for the insulation was 2.5 Btu/hr/sq ft/° F, and the gas film was 3.5 Btu/hr/sq ft/° F, the Oak Ridge figure is high, since the test was made outdoors. Another test, made indoors under ideal conditions, gave a U of 0.6 Btu/hr/sq ft/° F. Heat transfer coefficient for the insulation is assumed to be adequate to provide an over-all U of 1 Btu/hr sq ft/° F.

$$Q = U A \Delta T$$

$$A = \frac{2\pi (1.33)^2}{4} + \pi \times 1.33 \times 3.33 = 16.7 \text{ sq ft}$$

$$\text{Taking a cell temperature of } 140 \text{ F} \quad \Delta T = 572 - 140 = 432 \text{ F}$$

$$\text{therefore } Q = 1 \times 16.7 \times 432 = 7200 \text{ Btu/hr}$$

The total heat out of the pressurizer therefore is:

$$726 + 7200 = 7926 \text{ Btu/hr}$$

Heat InCondensate

The condensate would be preheated by heat exchange with the let-down liquid. This condensate will probably be preheated to 290 C (554 F).

$$4.31 \times 0.002205 \times 461 = 4.38 \text{ Btu/min}$$

Oxygen bleed-in

We assume the oxygen is to be introduced at the cell temperature of 140 F.

$$0.1125 \times (60 - 18) \times (7.0 - 2.0) \times \frac{1}{252} = 0.094 \text{ Btu/min}$$

Deuterium

0.0225 Btu/min (the same as D₂ out)

Heat flow through the liquid in the leg to the pressurizer and through the metal in the pipe is assumed to be zero, since essentially no temperature differential exists.

Total heat in from condensate, oxygen bleed-in and entering deuterium = $4.38 + 0.094 + 0.023 = 4.497$ Btu/min or 269 Btu/hr. The heat which must be supplied to keep the pressurizer isothermal at 300 C is:

$$7926 - 269 = 7657 \text{ Btu/hr}$$

If electrical heaters are employed to keep the pressurizers isothermal, a heater rating is calculated as follows:

$$7657 \times \frac{0.2930}{1000} = 2.24 \text{ kw heater or 2.5 kw heater per pressurizer is required.}$$

APPENDIX D

CALCULATIONS ON DUMP PROCEDURE

A. DUMP CALCULATIONS ASSUMING ISOTHERMAL CONDITIONS

When the reactor drains, boiling will not occur if the vapor pressure of the solution is less than the total pressure above the solution. With isothermal conditions at 280C, no radiolytic decomposition of heavy water, and recombination in accordance with Figure 2, we made the following calculations:

From Figure 2 the following points were taken to establish the equation for the unrecombined gas:

<u>Time (minutes)</u>	<u>Percent Recombined</u>	<u>Percent Remaining</u>
1	52	48
2	77	23
3	89	11

$-dc = kcd\theta$ where: c = concentration of dissolved radiolytic gas remaining

$-\frac{dc}{c} = kd\theta$ θ = time (minutes)

Integrating:

$-\ln \frac{c_2}{c_1} = k\theta$ c_1 = initial concentration of radiolytic gas

$+\ln \frac{c_1}{c_2} = k\theta$ c_2 = final concentration of radiolytic gas

$K^1 = \frac{\log \frac{c_1}{c_2}}{\theta}$ K^1 = constant for $\log_{10}$ (designated hereafter as log)

$$1 \text{ min } \frac{\log \frac{1}{0.48}}{1} = K^1 = 0.318$$

$$2 \text{ min } \frac{\log \frac{1}{0.23}}{2} = K^1 = 0.319$$

$$3 \text{ min } \frac{\log \frac{1}{0.11}}{3} = K^1 = 0.319$$

Equation for unrecombined gas present at any time is:

$$\log \frac{C_o}{C} = 0.32 \theta \quad \text{where } C_o = \text{original concentration} \\ C = \text{concentration at time } \theta$$

Henry's Law is $P = Kx$. Since K is constant, the ratio of concentration in the liquid is equal to the ratio of partial pressures. Thus:

$$\log \frac{(P_o)}{P} = K^1 \theta$$

$$\frac{P_o}{P} = 10^{K^1 \theta} \quad \text{or } P = P_o \times 10^{-0.32 \theta}$$

Under isothermal conditions (280C average temperature of solution) the boiling pressure (P_s) of the solution equals the vapor pressure of water plus the partial pressure of the uncombined ratiolytic gas.

$$P_s = 931 + P_o (10)^{-0.32 \theta} \quad P_o = \text{initial partial pressure of} \\ \text{noncondensable gases} = 750 \text{ psi}$$

Under isothermal conditions, total pressure (P_T) over the solution equals the vapor pressure of water at 300C (the temperature in the pressurizer), plus the partial pressure of the noncondensable gas during the dump. It is assumed that gas will not be steam-stripped from the solution by boiling and that complete humidification will take place from the surface of the discharging liquid.

$$P_T = 931 + P_o \frac{V_P}{(V_P + V_r \times \frac{\theta}{10})}$$

where P_o = initial partial pressure of noncondensable gases = 750 psi

V_P = total volume of core and blanket pressurizers = 180 l

V_r = total volume of core and blanket fluids 2240 l

θ = time in minutes (10-min total dump period assumed)

Table DI shows P_s and P_T for various periods of time.

TABLE DI

COMPARISON OF P_s AND P_T

<u>Time (minutes)</u>	<u>P_s (psi)</u>	<u>P_T (psi)</u>
0.0	1681	1681
0.5	1450	1393
1.0	1275	1265
2.0	1103	1146
4.0	970	1065
6.0	940	1020
10.0	931	987

When P_s is greater than P_T , mass boiling will occur. Thus boiling will occur during the first minute of the dump if the liquid is discharged immediately and if there is no delay to allow for recombination to reduce the boiling pressure of the solution. If the dump were delayed for even one-half minute, however, boiling would not occur as indicated in Table DII.

TABLE DIICOMPARISON OF P_s AND P_T WITH 1/2-MINUTE DELAY

<u>Time (minutes)</u>	<u>P_s (psi)</u>	<u>P_T (psi)</u>
0.0	1681	1681
0.5	1450	1681
1.0	1275	1393
2.0	1103	1202
4.0	970	1071
6.0	940	1026
10.0	931	989
10.5	931	987

In order for the dump to take place as calculated above, conditions must be isothermal at 280C, and humidification from the surface must be complete. The amount of humidification required is calculated as follows:

After the liquid has been discharged, the system is at 987 psi, of which 56 psi is the partial pressure of the original oxygen content of the pressurizer. Thus at the end of the liquid discharge, the vapor space of the core-and-blanket system - 2420 l occupied with steam at 280C and 931 psia and 0.482 cu ft per pound:

Final Steam

$$\frac{2420}{28.32 \times .482} = 178 \text{ lb of steam}$$

Initial Steam

$$\frac{180}{28.32 \times .482} = 13 \text{ lb of steam}$$

Therefore, the amount of water humidified during the dump is 165 pounds or 16.5 lb/min

$$\text{Heat required} = 165 \times 662.8 = 109,000 \text{ Btu}$$

Thus, 190,000 Btu must be supplied during the 10-min dump and 165 lb of steam must be vaporized by humidification from the surface of the discharging liquid. The heat, of course, must be supplied from some source such as the mass of confining equipment, after-heat from the nuclear reaction, and recombination of radiolytic gases. Humidification requires good mixing in the liquid phase, and since mixing will probably not be complete and material is being continually discharged from the system through the dump valves, the temperature will undoubtedly drop on the surface of the descending liquid. This drop in temperature will, of course, affect the humidification rate to a point where the pressure above the liquid will be reduced to less than the boiling point of the liquid below. When this occurs, mass boiling will take place, and the surface liquid will be preheated by condensing steam and violent mixing, with the result that the humidification rate will again increase. Thus at least intermittent boiling will occur. It is not possible, however, to determine to what extent mass boiling will occur and how much gas will be steam-stripped from the solution. Therefore, dump procedure must be designed so that even if boiling occurs, explosive concentrations of D_2 and O_2 will not develop in the system. More rigorous calculations than those presented above were carried out to determine the delay necessary for adequate recombination to insure safe dump procedure.

B. CALCULATIONS OF DUMP PROCEDURE ON LIMITING CONDITION OF COMPLETE POWER REJECTION

To accurately determine the time required to allow sufficient recombination to take place, so that any boiling in the reactor system will not create a vapor with a D_2 concentration greater than 1%, we present the following calculations, which take into account temperature variation with time, considering first the limiting condition of complete rejection of power.

The temperature of the core solution at any time after a pump stoppage (complete rejection of power) is given in Figure D1. This curve will be essentially the same if the steam throttle valve is completely closed. By establishing the relation of the constant in the rate equation for the recombination process with temperature we can determine the variation of rate with time.

$$-\frac{dc}{d\theta} = kc \quad (1)$$

where c = concentration of D_2

θ = time in minutes

k = rate equation constant

FIG. D1
TEMP. OF FUEL IN HRT CONE VS. TIME AFTER FUEL CIRCULATION STOPS.
AFTER ∞ OPERATION AT 5 MW

ASSUME NO HEAT LOSS

FISSION + FISSION PRODUCT ENERGY
(FROM CF-54-9-64)

Determination of variation of k with temperature:

Integration of the above gives

$$\log \frac{c_1}{c_2} = K^1 \theta \quad \text{where } K^1 = \frac{k}{2.3}$$

From Figure 2 $T = 280^\circ\text{C}$, 11% of the D_2 originally in the solution at $\theta = 0$ is present when $\theta = 3$ min.

$$\frac{c_1}{c_2} = \frac{1}{0.11}$$

$$K^1 = \frac{\log 9.1}{3} = 0.319$$

Also from Figure D2, when $T = 300^\circ\text{C}$, 10% of the original D_2 remains after 1.43 min

at 300°C K^1 is found to be 0.70

Assuming $\frac{\Delta}{20} = \frac{T-280}{20}$ where T is temperature $^\circ\text{C}$ above 280°C

$\frac{\Delta}{20}$	$\frac{K^1}{K^1_0}$
0	1
1	2.19
2	4.81

where K^1_0 is the initial value of constant.

These values are plotted on Figure D2, and the following equation results:

$$2.90 \log \frac{K^1}{K^1_0} = \frac{\Delta}{20} = \frac{T-280}{20}$$

$$\text{or } \log \frac{K^1}{K^1_0} = \frac{T-280}{58.6} \quad (2)$$

FIG. D2

From Figures D1 and D2, Figure D3 is evolved. Figure D3 shows the relation of K^1 with time. By means of this curve, one can determine the relationship of D_2 concentration with time.

$$-\frac{dc}{d\theta} = kc$$

$$-\int_1^2 \frac{dc}{c} = k \int_0^\theta d\theta$$

resulting in

$$\log \frac{c_1}{c_2} = K^1 \int_0^\theta d\theta \quad (3)$$

By graphical integration of Figure D3, we can determine the relationship of $\frac{c_1}{c_2}$ with time (θ). It is presented in Figure D4.

With concentration expressed as partial pressures, the relationship of partial pressure with time is developed.

$$P = \frac{k}{x}$$

where k = Henry's Law constant

$$P = kx$$

$$\frac{P_1}{P_2} = \frac{k_1}{k_2} \frac{x_1}{x_2}$$

FIG. D4

D-11

$$\text{where} \quad \frac{x_1}{x_2} = \frac{c_1}{c_2}$$

$$\text{therefore} \quad \frac{P_1}{P_2} = \frac{k_1}{k_2} \frac{c_1}{c_2}$$

$$\text{or } P_2 = \frac{P_1 \frac{k_2}{k_1}}{\frac{c_1}{c_2}} \quad (4)$$

The relationship of k with time is given in Figure D5, and is developed from values given in "Solubilities of He, H₂, N₂, O₂, and Xe in H₂O" by C. G. Heisig.

In order to slightly simplify the calculations, we assumed that radiolytic gas formation ceases at time $\theta = 0$. This assumption will cause very little error, since the nuclear reaction drops off quite rapidly when the throttle is closed or the pump is stopped. This drop in reaction is indicated by curves relating fission power to time.

The ratio of dissolved gas D₂ to total gas in the system at $\theta = 0$ is 71.1/76.75, with the partial pressure of D₂ equalling 500 psi. We obtain these values by conservatively assuming a solution temperature of 300C. The excess gas which is not in solution is present as small bubbles, and the partial pressure of the D₂ will not drop off until the bubbles are dissolved.

To determine the time required to dissolve the bubbles:

Assume a time of 5 seconds after $\theta = 0$

$$\text{when } \theta = 0 \quad C_1 = 76.75$$

$$\text{at 5 sec } \frac{c_1}{c_2} = 1.11$$

Therefore, $c_2 = 1$ which is below the dissolved gas concentration at the temperature of the solution when $\theta = 0$. The temperature of the solution raises, however, and increases the solubility, actually increasing the capacity of the solution for D₂. Assuming that the free gas will redissolve as soon as the solution will hold it all, the free gas will be dissolved within 5 seconds after stoppage.

FIG. D5

We therefore assume all gas is dissolved when determining partial pressure of D_2 with time.

Using equation (4), we can establish the partial pressure of D_2 in the solution at any given time (Figure D6). The time delay required before the dump can be begun so that any boiling or flashing of the solution, would result in a D_2 concentration of 1% or less is determined as follows:

We assume that the vapor pressure of D_2O remains at 1250 psi.

x = partial pressure of D_2

$$\frac{x}{1250 + x} = 0.01$$

$$x = 12.6 \text{ psi}$$

Figure D6 indicates that this partial pressure corresponds to 1.88 min after pump-stoppage or throttle-closure. Therefore, if the dump is delayed for 113 seconds, the D_2 concentration will not exceed 1% in even the first vapor evolved, should boiling occur during the dump.

The calculations presented above are for the limiting case of pump stoppage (complete rejection of power). This is actually not the conservative case from the standpoint of delay for recombination, since the temperature rise increases the recombination rate and thus decreases the required delay time.

VARIATION OF PARTIAL PRESSURE OF
DEUTERIUM IN CORE SOLUTION WITH
TIME AFTER COMPLETE REJECTION
OF POWER

C. CALCULATIONS ON DUMP PROCEDURE WITH SOME HEAT REMOVAL

It is believed that in the normal dump procedure some heat will be removed, so that there will be gradual rejection of power taken from the reactor. Calculations made by Rosenthal indicate that the average temperature in the reactor will rise only slightly during a normal dump falling back shortly to 280C. Thus, in order to be conservative, we believe that delay time for recombination should be based upon a 280-C average temperature being maintained.

It is assumed in the calculations to follow that just prior to the emergency dump, the reactor is operating normally, and that the initial conditions are based upon operation at 300C and 2000 psia liquid leaving the core.

Partial pressure of D_2 to O_2 in solution = 750 psia at 300C.

Assuming ratio $\frac{D_2}{O_2} = 2$, partial pressure of D_2 is

$$750 \times \frac{2}{3} = 500 \text{ psia partial pressure oxygen in solution.}$$

Henry's Law constant for H_2 @ 300 C is $K = 2.0 \times 10^5$

$$x = \frac{500}{2.0 \times 10^5} = 0.0025 \text{ or } 2.5 \times 10^{-3} \text{ gm moles } D_2/\text{gm mole solution.}$$

$$\text{Total volume of solution} = 490 + 1750 = 2240 \text{ l}$$

$$2240 \times 1000 \times 0.73 = 1,635,000 \text{ gm of solution}$$

$$\frac{1,635,000}{20} = 81,750 \text{ gm moles solution}$$

D_2 dissolved $81,750 \times 0.0025 = 204$ gm moles D_2

$$204 \times 22.4 \times \frac{14.7}{2000} \times \frac{573}{273} = 70.5 \text{ l } D_2 \text{ at } 300C \text{ } 2000 \text{ psia}$$

D_2 suspended assuming total suspended gas 1% of liquid volume

$$2240 \times 0.01 \times 0.20 = 4.5 \text{ l } D_2 \text{ at } 300C \text{ } 2000 \text{ psia}$$

Total amount of D_2 at 280C and 2000 psia is 72.5 l.

Using the same procedure employed in the isothermal calculations presented above, we give the amount of unrecombined radiolytic gas remaining in the liquid system in the table DIII, assuming that there is no discharge of gas from the system and that both the dissolved and suspended gas may be treated as dissolved gas. Time zero is taken as the time at which radiolytic gas production rate equals the recombination rate. We also assume that the radiolytic-gas production rate falls off so rapidly after time zero that further production of radiolytic gas is insignificant.

From the above data, one can conclude that the dump (release of liquid) should be delayed about 5 to 5 1/2 minutes in order to avoid developing deuterium concentrations of 1% or higher, should boiling occur. As mentioned previously, it is very important to establish the extent to which humidification of the vapor space from the liquid surface delays reduction of pressure in the vapor space to a point where mass boiling occurs in the liquid. This cannot be calculated with any degree of reliability and therefore should be determined experimentally in the early dump tests on the HRT.

The delay time can be shortened, however, if one considers the worst or most conservative limiting condition, i.e., straight adiabatic expansion of the vapor without humidification from the liquid surface. If the liquids discharge at a constant rate from the two dump lines such that the entire volume of liquid is discharged in 10 minutes, time will be required to allow for this reduction and the delay time may be shortened by this amount. From the data presented above, boiling should be delayed 5 minutes from the time radiolytic gas production falls below recombination. Calculation on the amount of time the delay may be shortened to allow for adiabatic pressure shows:

TABLE DIII

<u>Time Minutes</u>	<u>Partial Pres- sure Steam at 280C.</u>	<u>Partial Pres- sure Radio- lytic Gases</u>	<u>Boiling Pres- sure of Solu- tion</u>	<u>Partial Pressure D2</u>	<u>Amount D2 Liters 280C. 2000 psia</u>	<u>D2 Content of First Vapor if Boiling Occurs</u>
0	931	750.0	1,681	500	72.5	26.1%
1	931	344.0	1,275	229	33.2	15.2%
2	931	172.0	1,103	115	16.7	8.6%
3	931	82.5	1,014	55	8.0	4.4%
4	931	39.0	970	26	3.8	2.2%
5	931	20.0	951	13	1.9	1.1%
6	931	9.0	940	6	0.9	0.5%

$$\frac{P_1}{P_2} = \left(\frac{V_2}{V_1} \right)^k$$

$$\text{where } k = \frac{C_p}{C_v}$$

P_1 = Initial pressure (2000 psia)

V_1 = Initial Vapor Volume (180 l)

P_2 = Boiling Pressure of Liquid to yield no more than 1% D2 vapor, (946 psia)

V_2 = Vapor Volume corresponding to 946 psia.

$k = 1.28$ for H_2O (Approx.)

$k = 1.40$ for O_2 (Approx.)

$k = 0.70 (1.28) + 0.30 (1.40) = 1.32$

$$\frac{2000}{946} = \left(\frac{V_2}{180} \right)^{1.32}$$

$$(2.11)^{0.758} = \frac{V_2}{180}$$

$$V_2 = 317 \text{ l}$$

Amount of liquid discharged = $317 - 180 = 137 \text{ l}$

Since 2240 l will be discharged in 10 min, it will require:

$$\frac{137}{2240} \times 10 = 0.61 \text{ min or } 37 \text{ sec}$$

Therefore, the minimum delay required before opening dump valves would be $5 \text{ min } 30 \text{ sec} - 37 \text{ sec} = 4 \text{ min } 53 \text{ sec}$.

APPENDIX E

PRELIMINARY DESIGN OF LIGHT-HYDROGEN RECOMBINER

A. ASSUMPTIONS

1. Oxygen is 80% reacted per pass.
2. One cubic foot per hour (STP) is bled from the reactor and has an oxygen content of 0.5%.
3. Water formed in the reaction is withdrawn as condensate.
4. The cooled gas is saturated at 170F.
5. Vessel diameter is 4 ft, the flow areas for the hot and cold gases being equal.
6. Pressure in recombiner is held at 1/4 psi.

The recombiner is basically designed for the maximum flow rate of oxygen, which occurs during a dump. In order that all the oxygen be removed within an hour, 154.5 SCF of oxygen must be united with hydrogen.

B. MATERIAL BALANCE

O_2 in = 154.5 SCF/hour or 0.43 lb moles/hour.

O_2 out = $1 \times 0.005 = 0.005$ SCF/hour.

H_2O out = $0.43 \times 2 = 0.86$ lb moles/hour.

Since only 80% of the oxygen is reacted per pass, a specific recirculation rate will be required in order that the required recombination can be obtained.

C. RECIRCULATION RATE

Let n = SCF/hour O_2 feed = 154.5

	Σ	
O_2 recirculated after first pass	= 0.20 n	0.20 n
O_2 " " second "	= 0.20 x 0.20 n	0.04 n
O_2 " " third "	= 0.20 ³ n	0.008 n
O_2 " " fourth "	= 0.20 ⁴ n	<u>0.0016 n</u>
	Σ =	0.25 n

The effect of the bleed-out stream is rather small and is therefore left out so that calculations can be simplified

$$0.25 (154.5) = 38.6 \text{ SCF of } O_2 \text{ recirculated per hour.}$$

$$\frac{38.6}{0.005} = 7720 \text{ SCF/hr total volume of gas recirculated in recombiner per hour.}$$

This corresponds to a gas velocity of:

$$\frac{7720}{6.3} \times \frac{14.7}{14.95} \times \frac{630}{492} \times \frac{1}{3600} = 0.43 \text{ ft/sec at } 1/4 \text{ psi and } 170F$$

Partial pressure of H_2O at 170F = 5.992

$$\text{Mole fraction of } H_2O \text{ in recycle gas} = \frac{5.992}{14.95} = 0.401$$

The composition of the gas being recirculated, therefore, is:

$$H_2O \quad 7720 \times 0.401 = 3090 \text{ SCF/hr}$$

$$O_2 \quad 7720 \times 0.005 = 39 \text{ SCF/hr}$$

$$H_2 \quad 7720 - 3129 = 4591 \text{ SCF/hr}$$

From these figures are developed the compositions of the gas being recirculated at 170F, the gas as it enters the catalyst bed, and the gas as it leaves the bed. These compositions are given in Table EI.

D. HEAT BALANCES

A temperature of 170F was assumed for the gas after it is cooled. This gas will then be preheated to 200F just prior to its entering the catalyst bed.

The temperature rise to be expected in the catalyst bed during a dump is:

$$1/2 \text{ O}_2 + \text{H}_2 \longrightarrow \text{H}_2\text{O(g)} \triangle H_c = 103,142 \text{ Btu/lb mole at 77F.}$$

$$\triangle H_{c200} = 103,142 + 123 \left[1(8.2) - 1(7.0) - 0.5(7.1) \right]$$

$$\triangle H_{c200} = 102,859 \text{ Btu/lb mole}$$

The specific heats used are approximate averages and are sufficiently accurate for these preliminary design calculations.

C_p taken as average between 720F and 200F:

$102,859 \times 0.86 = 88,500 \text{ Btu/hr heat release by recombination process}$

$$Q = \triangle T (M_{\text{H}_2} C_p + M_{\text{H}_2\text{O}} C_p + M_{\text{O}_2} C_p)$$

$$88,500 = \triangle T \left[12.79(7.0) + 9.50(8.6) + 0.109(7.7) \right]$$

$$\triangle T = \frac{88,500}{172} = 515\text{F}$$

$$T \text{ at top of bed} = 200 + 515 = 715\text{F}$$

E. STEAM COILS

Steam is available at 250 psia, $T = 401\text{F}$

Gas in = 170F

Gas out = 200F

$$\triangle T_{1m} = \frac{(401 - 170) - (401 - 200)}{\ln \frac{(401 - 170)}{(401 - 200)}} = 214\text{F}$$

TABLE EI

STREAM COMPOSITIONS

	<u>Recirculated Gas</u>			<u>Gas Entering Catalyst Bed</u>			<u>Gas Leaving Catalyst Bed</u>		
	<u>Moles</u> <u>per</u> <u>Hour</u>	<u>Mole</u> <u>Fraction</u>	<u>Pounds per</u> <u>Mole of Gas</u>	<u>Moles</u> <u>per</u> <u>Hour</u>	<u>Mole</u> <u>Fraction</u>	<u>Pounds per</u> <u>Mole of Gas</u>	<u>Moles</u> <u>per</u> <u>Hour</u>	<u>Mole</u> <u>Fraction</u>	<u>Pounds per</u> <u>Mole of Gas</u>
H ₂ O	8.64	0.401	7.21	8.64	0.378	6.80	9.50	0.424	7.64
O ₂	0.109	0.005	0.08	0.539	0.024	0.38	0.109	0.005	0.08
H ₂	<u>12.79</u>	<u>0.594</u>	<u>1.19</u>	<u>13.65</u>	<u>0.598</u>	<u>1.20</u>	<u>12.79</u>	<u>0.571</u>	<u>1.14</u>
<u>Total</u>	21.539	1.000	8.48	22.829	1.000	8.38	22.399	1.000	8.86

Assume $U = 3.5 \text{ Btu/hr/sq ft/}^\circ\text{F}$, calculated for flow of gas at right angles to tubes.

Heat Load =

$$Q = (200 - 170) \left[8.64(8.2) + 13.65(7.0) + 0.539(7.1) \right]$$

$$Q = 5100 \text{ Btu/hr}$$

$$\text{Heat Transfer Area} = \frac{5100}{3.5 \times 214} = 6.8 \text{ sq ft (use 8 sq ft)}$$

Length of 1/2-in. OD tubing =

$$\frac{8}{\pi/24} = 61.4 \text{ ft}$$

Avg diameter per turn = 1.42 ft

Avg length per turn = $1.42 \times \pi = 4.45 \text{ ft}$

$$\text{No. of turns} = \frac{61.4}{4.45} = 14$$

F. COOLING COILS

Assume water is available at 70F, $\Delta T = 15\text{F}$

$U = 4.5 \text{ Btu/hr/sq ft/}^\circ\text{F}$ as calculated for gas

flowing at right angles to tubes.

Heat to be removed = $5100 + 102,829 = 107,959 \text{ Btu/hr}$

$$\text{Water rate} = \frac{107,959}{15 \times 8.33 \times 60} = 14.4 \text{ gal/min}$$

$$\Delta T_{1m} = \frac{(7.15 - 85) - (170 - 70)}{\ln \frac{630}{100}} = 288\text{F}$$

$$\text{Area} = \frac{107,959}{4.5 \times 288} = 83.4 \text{ sq ft, (use 100 sq ft)}$$

Length of pipe (1/2 in. OD) required

$$\frac{100}{\pi/24} = 764 \text{ ft}$$

The average diameter between turns = 3.42 ft

$$\text{No. turns} = \frac{764}{3.42 \times \pi} = 71.2$$

With 5 turns maximum in a row, 15 rows are required.

In order to determine the stack height required to give the proper circulation, we must determine the pressure drops in the system.

The resistance to flow developed in the catalyst bed is found by:

$$\Delta p = 3.19 R_e^{-0.266} \frac{L S p V_o^2}{2 g_c F^3}$$

where:

Δp = pressure drop, lb force/sq ft

$$R_e = \frac{G_o}{\mu S}$$

G_o = mass flow rate, lb mass/sq ft - sec

μ = viscosity, lb mass/ft - sec

S = area per unit volume of bed, sq ft/cu ft

L = bed depth, ft

ρ = fluid density, lb mass/cu ft

V_o = velocity based on empty cross section, ft/sec

g_c = conversion factor, lb force - ft/lb mass-sq sec

F = void fraction of bed, dimensionless

$$L = 1/3 \text{ ft}$$

$$S = 100 \text{ sq ft/cu ft}$$

$$G_c = 32.2 \frac{\text{lb mass-ft}}{\text{lb force-sq sec}}$$

$$F = 0.97 \text{ (void fraction)}$$

Using the average bed temperature: $(200 + 715)/2 = 458\text{F}$.

$$V_o = 0.43 \times \frac{918}{630} = 0.63 \text{ ft/sec}$$

Using the moles of gas leaving the recombiner:

$$G_o = \frac{22.4 \times 8.86}{3600 \times 6.3} = 0.00876 \text{ lb mass/sq ft sec}$$

The average molecular weight of the gas in the catalyst bed =

$$\frac{8.86 + 8.38}{2} = 8.62$$

Average temperature in the bed =

$$\frac{715 + 200}{2} = 458\text{F}$$

Average density in the bed =

$$\rho = \frac{8.62}{359} \times \frac{492}{918} \times \frac{14.95}{14.7} = 0.0131 \text{ lb/cu ft}$$

$$\mu = 0.017 \times 0.000672 = 0.114 \times 10^{-4} \text{ lb mass/ft-sec}$$

We used the viscosity of steam to stay on the conservative side.

$$R_e = \frac{8.76 \times 10^{-3}}{0.114 \times 10^{-4} \times 10^2} = 7.68$$

Although the above Reynolds number is below 50 (the limit set on the previously given equation) the use of this equation at these lower Reynolds numbers should not introduce too great an error.

$$R_e^{-0.266} = 7.68^{-0.266} = 0.582$$

$$\Delta P = 3.19 (0.582) \frac{1/3 \times 100 \times 0.0131 \times (0.63)^2}{2 (32.2) \times (0.97)^3}$$

$$\Delta P = 0.0056 \text{ lb/sq ft}$$

Next we calculate the pressure drop across the cooling tubes using the equation:

$$\Delta P = \frac{4f N_r \rho V_{\max}^2}{2 g_c}$$

$$N_{R_e} = \frac{D_c V_{\max}}{\mu}$$

where:

ΔP = pressure drop, lb/sq ft

f = friction factor, dimensionless

g_c = conversion factor, lb mass-ft/lb force-sq sec

N_r = number of rows of tubes normal to the flow, dimensionless

$V_{\max}$ = fluid velocity through minimum area, ft/sec

ρ = density lb mass/cu ft

μ = viscosity, lb mass/ft sec

D_c = transverse clearance, ft

D_c = $\frac{1}{24}$

N_r = 15

$$V_{\max} = 0.43 \times \frac{1175}{630} \times \frac{1}{0.5} = 1.6 \text{ ft/sec}$$

$$\rho = \frac{8.86}{359} \times \frac{492}{903} \times \frac{14.95}{14.7} = 0.0137 \text{ lb/cu ft}$$

$$\mu = 0.114 \times 10^{-4} \text{ lb mass/ft sec}$$

$$N_{R_e} = \frac{1.6 \times 0.0137}{24 \times 0.114 \times 10^{-4}} = 80$$

$F = 0.055$ (Perry) Fig. 33B p. 392

$$\Delta P = \frac{4 \times 0.055 \times 15 \times 0.0137 \times 1.6^2}{2 \times 32.2}$$

$$\Delta P = 0.0018 \text{ lb force/sq ft}$$

The remainder of the resistances in the system are taken equal to this resistance; therefore the total $P = 0.0056 + 0.0018 + 0.0018 = 0.0092 \text{ lb/sq ft}$.

The driving force to overcome this resistance is obtained by a chimney effect which uses the differences in the densities of the hot and cold columns of gas. Density of the gas in the cooler column:

$$T_{lm} = \frac{715 - 170}{\ln \frac{715}{170}} = 380\text{F}$$

$$\text{Assume avg molecular wt} = \frac{8.48 + 8.86}{2} = 8.67$$

$$\rho = \frac{8.67}{359} \times \frac{14.95}{14.7} \times \frac{492}{840} = 0.0144 \text{ lb/cu ft}$$

Density of the gas in the warmer column

$$T = 715\text{F}$$

$$\rho = \frac{8.86}{359} \times \frac{14.95}{14.7} \times \frac{492}{1175} = 0.0105 \text{ lb/cu ft}$$

$$\Delta P = \Delta \rho h \left(\frac{g}{gc} \right)$$

$$\Delta \rho = 0.0144 - 0.0105 = 0.0039 \text{ lb/cu ft}$$

$$h = \frac{.0092}{.0039} = 2.36 \text{ ft (height of stack in recombiners)}$$

The recombiner should be designed so as to function properly at the oxygen rate that will be encountered under normal conditions. It is also necessary that at oxygen additions less than normal, the unit will function properly and the O_2 concentration will not build up above the 0.5% level.

If there is no oxygen flow (the extreme case) convection currents will be created only by steam and cooling coils. It will be necessary to reduce the cooling-water rate to prevent the temperature in the recombiner from becoming too low. Such a system can be controlled by sensing the temperature of the gas below the cooling coils and controlling the cooling-water flow rate. We can determine if circulation rate without heat evolution by reaction is sufficient to keep circulation to a point such that if O_2 were flowing under normal operating conditions, O_2 concentration in the bleed stream will not exceed 0.5%.

$$O_2 \text{ flow rate} = 5.36 \text{ SCF/hr}$$

$$\frac{0.25 (5.36)}{.005} = 268 \text{ SCF/hr}$$

neglecting bleed-out stream.

$$\frac{268}{6.3} \times \frac{14.7}{14.95} \times \frac{630}{492} \times \frac{1}{3600} = 0.0149 \text{ fps}$$

at 1/4 psi and 170F.

The pressure drop across the catalyst bed is reduced by the factor:

$$\frac{(V_2)^{2-0.266}}{(V_1)^{2-0.266}} \frac{(\rho_2)}{(\rho_1)} \frac{(\mu_1)}{(\mu_2)}$$

where subscript 1 refers to conditions during dump, and

subscript 2 refers to normal operating conditions.

Assume density and viscosity at 350 under normal conditions:

$$\frac{(0.0149)^{1.734}}{(0.62)} \frac{(.0149)}{(.0131)} \frac{(0.114 \times 10^{-4})^{-0.266}}{(0.102 \times 10^{-4})} = .00171$$

$$\Delta P = .0056 (.00171) = 0.957 \times 10^{-5}$$

To compensate for other resistances in the recombiner we assume the ΔP to be twice that through the catalyst.

$$\Delta P = 2 \times 0.957 \times 10^{-5} = 1.914 \times 10^{-5}$$

We now determine the ΔP driving force under the no oxygen flow condition to see whether this driving force is sufficient to overcome the resistance.

The temperature rise across the steam coils is:

$U = 2 \text{ Btu/hr/sq ft/}^\circ\text{F}$ (roughly the transfer coefficient at the reduced flow rate as calculated for flow at right angles to tubes.)

By trial and error methods we can determine the heated gas temperature.

Assuming exit-gas temperature of 364F:

$$\Delta T_{lm} = \frac{(401 - 170) - (401 - 388)}{\ln \frac{231}{13}} = 75.8^\circ\text{F}$$

Heat transferred to gas = $2 \times 8 \times 75.8 = 1210 \text{ Btu/hr}$

The calculated temperature of the heated gas is:

$$Q = \Delta T \left[0.75 (0.378 \times 8.2 + 0.598 \times 7.0 + 0.024 \times 7.1) \right]$$

$$Q = T (5.6)$$

$$T = \frac{1210}{5.6} = 216$$

$$T = 170 + 216 = 386^\circ\text{F} \text{ compared with } 388^\circ\text{F}$$

Therefore, the gas in the hot stack is at 388F.

Density of cooler column of gas:

Average gas temperature

$$T_{lm} = \frac{388 - 170}{\ln \frac{388}{170}} = 264^\circ\text{F}$$

$$\rho = \frac{8.48}{359} \times \frac{14.95}{14.7} \times \frac{492}{724} = 0.0163 \text{ lb/cu ft}$$

Density of hotter column of gas:

$$\rho = \frac{8.48}{359} \times \frac{14.95}{14.7} \times \frac{492}{848} = 0.0139 \text{ lb/cu ft}$$

$$\Delta \rho = 0.0163 - 0.0139 = 0.0024 \text{ lb/cu ft}$$

$$\text{Driving force} = 0.0024 (2.36) = 5.65 \times 10^{-3} \text{ lb/sq ft}$$

The driving force is therefore sufficient to maintain the required recirculation for normal operation with the steam coils acting as the sole heat source.

Specifications for Catalytic Recombiner:

Over-all Diameter	4 ft
Stack Diameter	2 ft - 10 in.
Effective Stack Height	2 ft - 4-1/2 in.
Cooling Coils (1/2 OD Tubing)	764 ft
Steam Coils (1/2 OD Tubing)	61.4 ft

APPENDIX F

FLOW SHEETS

MATERIALS LIST					
ITEM NO.	DWG. NO.	QTY.	NAME	SIZE	MATERIAL

d	General	4-15-57	F.C.E.	W.M.S.
c	General	3-7-54	F.C.E.	W.M.S.
b	General	7-19-55	F.C.E.	W.M.S.
a	General	4-30-55	F.C.E.	W.M.S.
NO.	REVISIONS	DATE	APPR.	APPR.
DRAWING NO. D-				
DRAWN	DATE	CHECKED	DATE	APPROVED
C.W. DAY	4-17-57	W.M.S.	4-21-57	W.M.S.
DESIGNED	DATE	SUBMITTED	DATE	APPROVED
F.C.E.	4-1-57	4-1-57	4-1-57	W.M.S.
HEALTH PHYSICS	MEDICAL	OPERATIONS	SAFETY	
FIRE PROTECTION	RESEARCH SHOP	MAINTENANCE		
REFERENCE DWGS				
JWS NO.				

THIS DRAWING
AS CLASSIFIED
PER 1-1-57
SCALE:

4 RT BLDG 7500

FLOW SHEET
HIGH PRESSURE FUEL SYSTEM

OAK RIDGE NATIONAL LABORATORY
OPERATED BY
CARBIDE AND CARBON CHEMICALS COMPANY
A DIVISION OF UNION CARBIDE AND CARBON CORPORATION
OAK RIDGE, TENNESSEE

SUBMITTED: 4-1-57
APPROVED: 4-1-57
REV: 1
D-18296 d

MATERIALS LIST					
ITEM NO.	DWG. NO.	QTY.	NAME	SIZE	MATERIAL

d	General	4-6-57	FCZ	WFB
c	General	3-7-56	FCZ	WFB
b	General	7-22-55	FCZ	WFB
a	General	4-33-55	FCZ	WFB

NO.	REVISIONS	DATE	APPR.	APPR.
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DRAWING NO. D-

DRAWN C.W. DAY	DATE	CHECKED	DATE	APPROVED W. R. Hill	DATE 3-2-55
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DESIGNED	DATE	SUBMITTED	DATE	APPROVED	DATE
		F.C ZAPP	2-22-55	W. P. Walker	3-21-55

HEALTH PHYSICS	MEDICAL	OPERATIONS	SAFETY
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FIRE PROTECTION	RESEARCH SHOP	MAINTENANCE	
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V	2	40
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HET Cooling Water Flowchart	D-18240
HET Multi Purpose Instruments	D-8360
HET Inst. Acquisition Information	A2094A
HET Flowchart Identification Code	D-18930
HET Flowchart Link Schedule	1-18329
Low Press. Blanket System Flowchart	D-18290
High Press. Cool System Flowchart	D-18296
Low Press. Fuel System Flowchart	D-18295
Steam System Flowchart	D-18261
DEPENDENCE DWGS.	DWG. NO.

N.R.T. BLDG. 7500

FLOWSHEET
HIGH PRESSURE BLANKET SYSTEM

OAK RIDGE NATIONAL LABORATORY
OPERATED BY
CARBIDE AND CARBON CHEMICALS COMPANY
A DIVISION OF UNION CARBIDE AND CARBON CORPORATION
OAK RIDGE, TENNESSEE

SUBMITTED	ACCEPTED	APPROVED	REV
	5/13/58 <i>Boyle</i>	<i>Boyle</i> 3-10-58	
		D-18297	d
57	58	59

DC+ BUS 4

48 VOLTS -

CORE SIDE
OXYGEN BLEED-IN
SOLENOID. CLOSES
LINE 182 WHEN
DE-ENERGIZED

BLANKET SIDE
OXYGEN BLEED-IN
SOLENOID. CLOSES
LINE 282 WHEN
DE-ENERGIZED

TITLE <u>SUPPLEMENT TO DWG E-21020</u>					
(HRT CONTROL CIRCUIT FUNCTIONAL SCHEMATIC - SHEET 1)					
CLIENT			LOCATION		
PLANT			LOCATION		
APPD.			DRW. FJM	CHRD.	SCALE
			DATE 2/14/1		
LIC. ENT.			B-60034 - 3		

APPENDIX GBIBLIOGRAPHY

1. T. H. Pigford, Explosion and Detonation Properties of Mixtures of Hydrogen, Oxygen, and Water Vapor.
2. N. A. Lange, Handbook of Chemistry, August 14, 1952.
3. "Fire Hazard Properties of Certain Flammable Liquids, Gases, and Volatile Solids," National Fire Protection Association.
4. Bone and Townsend, Flame and Combustion in Gases.
5. Bureau of Mines Bulletin 279, p. 17.
6. Von Elbe, private communication.
7. Battelle Memorial Institute reports.
8. Hill (CF-54-9-64)