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DETERMINATION OF OPTIMAL LWR CONTAINMENT DESIGN,
EXCLUDING ACCIDENTS MORE SEVERE
THAN CLASS B

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L. Cave
T. K. Min

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DETERMINATION OF OPTIMAL LWR CONTAINMENT DESIGN,
EXCLUDING ACCIDENTS MORE SEVERE THAN CLASS 8

L. Cave
T. K. Min

Prepared by

Chemical, Nuclear and Thermal Engineering Department
School of Engineering and Applied Science
University of California
Los Angeles, California 90024

Prepared for

U. S. Department of Energy
Light Water Reactor Safety Technology Management Center
Sandia National Laboratories
Albuquerque, New Mexico 87185

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SUMMARY

The study described in this report was commenced some months before the accident to the Three Mile Island No. 2 plant (TMI 2). As the full analysis of that accident has not yet been completed, no attempt has been made to include any detailed consideration of its implications on containment design. However, where it appears that it may have a significant effect on the assumptions made in this report, or on the conclusions reached, this has been indicated in the text.

The first stage in the study was to attempt to define realistically the performance required from a containment in accident classes 3 through 8. This definition presents some difficulties as the existing main documentary guidance (Nuclear Regulatory Commission Regulatory Guides 1.3 and 1.4) contain assumptions about the extent of damage to the fuel which, prior to the TMI 2 accident would have been considered unduly conservative for an accident in classes 3 to 8. It should be noted that throughout this study it has been assumed that the accidents remain within the "design basis" (i.e. all the engineered safety system function correctly). For convenience, these are described as "well conditioned" accidents. There are also a number of other aspects about which conservative assumptions have been made. However, the documentary guidance contains assumptions about the level of radiation exposure, to the public, on which containment designs should be based that might be regarded as unduly high for accidents which are within the design basis envelope.

This preliminary study of the requirements led to two conclusions:

- (a) That, if the source terms in the Regulatory Guide were retained, there was little scope for a reduction in containment performance

from that required in current plants and thus, at first sight, little scope for reducing cost.

- (b) That, if an attempt was made to reduce the conservatism in the source terms and elsewhere, this would need to be accompanied by a re-consideration of the acceptable levels of radiation exposure to the public.

Thus, the second stage in the study was to review systematically all the possible sources of conservatism in the assumptions and methods described in the relevant guidance documents. It was found that the methods and assumptions in the appropriate Regulatory Guides gave rise to an overall factor of conservatism in the estimated dose of order 10, apart from the source term; including the source term this factor may be as large as order 10^4 . However, if the assumptions and methods used in the Environmental Report Models were used, this factor was considerably smaller, about unity, apart from the source term, and of order 10^2 including this term.

The third stage in the study was to formulate an alternative set of radiation exposure criteria, suitable for use with a "best-estimate" set of assumptions and methods for assessing the radiation exposures from Class 3 through 8 accidents. Clearly there is an almost infinite variety of criteria that could be proposed. For the purposes of this study, however, it was sufficient to develop a set which was likely to be the most severe that would be required by any of the interested parties. The set of criteria proposed has been developed from the concept that it would be desirable to show that, for accident Classes 3 through 8, there would be no interruptions of the public's activities

due to faults on the reactor. It so happens that the particular set of criteria chosen for this limiting case proved to be so restrictive that they virtually offset the overall conservatism estimated to be present in the assumptions and methods of the Regulatory Guides. Consequently, if this set of criteria were adopted, there would again be little scope for reducing the performance required in current containment designs.

Although a simple cost/benefit approach, based on the "expectation of loss" principle, indicated that there was a case for dispensing with containment, this line was considered to be so unlikely to be acceptable that it was not pursued further and attention was focussed on the possibility of meeting current performance standards at lower cost.

The fourth stage of the study, therefore, was an analysis of the rather limited data available for the cost of the main elements in typical containment systems and an investigation of the cost/performance relationship. From these studies it appeared that the most promising method of reducing the cost of containment for PWRs lay in the exploitation of the pressure suppression concept, since this could lead to lower design pressures and the possibility of dispensing with low leak rate steel liners. Estimates of the cost of the Westinghouse "ice containment" indicated that this should be about 15 percent lower than that of the conventional "dry" containment system. Estimates of the cost of the CANDU-PHWR containment system, based on the very rapid condensation of steam by the use of a rapid dousing system and the replacement of the conventional steel liner by a plastic coating, indicated that a reduction of 25 percent in the cost of the containment system for a 1100 MW(e) PWR. In principle the ice containment system could be developed further to permit the use of plastic liners, which should lead to an

overall saving similar to that estimated for the Canadian system. However, although PWRs using the ice-condenser concept are already in use, it is not entirely clear that the difficulties encountered with the system are due to remediable design errors or to more generic problems; moreover, the acceptability in the US of the Canadian system has not yet been tested. Thus it is not yet certain that these potential reductions in containment cost can be achieved in practice.

The analysis of costs indicated also that, if an improvement over present standards of performance were required, this could best be achieved by the use of the double containment concept, with "pump back" from the annular space into the inner vessel. By the use of plastic lined pre-stressed concrete shells, together with the pressure suppression concept, the overall cost should be about the same as that of the Reference Design.

In the case of BWR, as a pressure suppression system was used in the Reference Design, there is less scope for a reduction in cost. However, the Reference Design for a BWR containment system appears to contain some unduly expensive features and reductions of between 7 and 15 percent might be achievable by adopting the same approach as that proposed above for PWR containment systems. Similarly, the performance could be improved at little extra cost by an application of the double-containment concept.

Savings of up to \$15 m (at 1979 prices) per PWR appears to be attainable; development work (e.g., on the reliability and performance of some pressure suppression systems and on the testing of plastic liners) would be necessary to achieve these savings. However, it is not clear

that, in the aftermath of the TMI-2 accident, utilities would be interested in making savings of this order, if it were necessary to depart from accepted design solutions in order to achieve them; nor is it clear whether changes in containment performance requirements will be formulated by NRC, or the utilities themselves, (e.g., there may be requirement to cater for core meltdown situations). If major changes in requirements are proposed these may interact with the methods visualized for reducing containment costs. Thus there are two areas in which more work is required before a decision could be made to embark on a development program aimed at reducing the cost of containment. If it were decided to go ahead with a program of development to support cost reduction, the main items would be:

- (a) Investigation of the reliability of pressure suppression systems, as compared with pressure retention systems
- (b) Analysis of all available experience with ice containments, to determine whether the system is subject to any unresolved generic problems
- (c) Investigation of the performance of the Canadian rapid dousing pressure suppression system
- (d) Review of data relating to the leak rates of unlined and plastic lined pre-stressed concrete shells
- (e) Continuation of possible methods for reducing the cost of protecting interior concrete structures of BWR containments.

(1) Introduction

For many years the retention performance of containment systems for LWR's has been determined largely by the requirements set out in Regulatory Guides 1.3 and 1.4 (Ref. 1 and 2). These requirements are discussed in detail later, it is only necessary for the moment to note that they require the applicant for a license to assume that large proportion of the gaseous and volatile fission products have been released from the fuel; these proportions are, in fact, so large that they could only occur as a result of prolonged heating of the fuel at temperatures greater than 1500°C or of core melt down. However, for accidents in Classes 1 through 8, as defined in Ref. 3 for the purposes of preparing Environmental Reports, the amounts of activity released are consistent with fuel cladding failure accompanied by only a small increase in fuel temperature, far short of melting.

Preliminary calculations, based on recent FSAR's for PWR's with "dry" containments, have shown that if the conservative assumptions required by Regulatory Guide 1.4 are employed, there is relatively little margin of safety in the containment performance (factors of 1.6 to 3.3, according to reactor design). Thus there would be little opportunity for modifying the containment design in order to reduce the cost.

However, if a situation is visualized in which the Regulatory Guides were revised and less conservative assumptions were used for accidents in Class 1 through 8, then there could be scope for changes in the containment system. Nevertheless, in this more realistic situation it would probably be necessary to revise downwards the

maximum dose delivered to members of the public and to consider other possible restrictions on containment design such as the need for the structure to resist seismic forces.

Thus three necessary stages in the first part of this study are as follows:

- (a) An evaluation of the degree of conservatism in the requirements of Reg. Guides 1.3 and 1.4,
- (b) Consideration of appropriate dose limits for members of the public, taking account into the expected frequencies of the accidents in Classes 3 through 8,
- (c) Consideration of the constraints imposed on the containment design by other factors, such as the need to withstand external hazards.

The performance and cost of a variety of containment designs for water reactors, some of which have not yet been used for commercial reactors in the United States, have been examined. In addition some possible ways of reducing costs, while meeting either the Reg. Guide requirements for radiation exposure of the public, or some other hypothetical requirements, have been examined in more detail to establish the extent of the saving which might be made.

This report is divided into eight sections. Following this introduction, Section 2 reviews and evaluates the extent of the conservatism in NRC requirements. Section 3 defines the possible targets for containment performance and summarizes containment performance requirements to be considered in optimization studies. Section 4 outlines the possible containment systems for Light Water Reactors (LWR's).

Section 5 gives the optimization of containment design for Classes 3 through 8 for Pressurized Water Reactors (PWR's). Section 6 describes the estimated costs of some possible arrangements for PWR relative to the standard dry containment system (Ref. 4). Section 7 also gives the estimated costs of Boiling Water Reactors (BWR's). Sections 8 and 9 contain the conclusion of this study. More detailed explanations for each section are presented in Appendices at the end of this report. The more important types of containment considered in this study are illustrated in Figures 1.1 through 1.5.

References of Section 1

- (1). U.S. Nuclear Regulatory Guide 1.3, "Assumptions used for evaluating the potential radiological consequences of a loss of coolant accident for boiling water reactors," Revision 2, June, 1974.
- (2). U.S. Nuclear Regulatory Guide 1.4, "Assumptions used for evaluating the potential radiological consequences of a loss of coolant accident for pressurized water reactors," Revision 2, June, 1974.
- (3). U.S. Nuclear Regulatory Guide 4.2, "Preparation of Environmental Reports for Nuclear Power Stations," NUREG-0099, Revision 2, July, 1976.
- (4). "Capital Cost: Pressurized Water Reactor Plant," NUREG-0241, C00-2477-5, June, 1977.

Scale 1" = 32.5'

Figure 1.1 Typical PWR dry containment.

1 - ANNULUS	13 - PIPE PENETRATION AREA
2 - CONTAINMENT	14 - PRESSURE RELIEF VENT
3 - TROLLEY	15 - REACTOR COOLANT PUMP
4 - BRIDGE RAIL	16 - REACTOR
5 - OPERATOR'S CAB	17 - WALKWAY
6 - MAIN STEAM LINE	18 - PIPE CHASE
7 - SAFETY INJECTION TANK	19 - EQUIPMENT DRAIN TANK
8 - CABLE TRAYS	20 - AUX IDW PUMP
9 - MISSILE SHIELD	21 - IN CORE 1" SI SUMP and PUMP BOTTOM
10 - CIDM	22 - RECIRC SUMP VALVES
11 - STEAM GENERATOR	23 - CONT SPRAY PUMP
12 - ELECT CHASE	

Scale 1" = 25.5'

Figure 1.2 Elevation view of a typical spherical PWR containment.

Figure 1.3 ICE CONDENSER CONTAINMENT

Figure 1.4 Mark III BWR Reactor Building

1 INTERNAL STRUCTURE	8 MONORAIL AND HOIST
2 IMPEDECT WATER STORAGE TANK	9 PERSONNEL AIRLOCK
3 DISTRIBUTION AND SPRAY HEADERS	10 EQUIPMENT AIRLOCK
4 VACUUM PIPE	11 SERVICE TUNNEL
5 VALVE MANIFOLD	12 CATCH BASIN
6 PRESSURE RELIEF VALVE	13 ROOF/WALL SEAL
7 PRESSURE RELIEF DUCT	14 BASEMENT

Figure 1.5 Cutaway view of vacuum building and valve manifold, Pickering (CANDU)

(2). The restrictive effect of existing NRC requirement

(2.1) Description of the NRC Requirements

<2.1.1> Regulatory Guide 1.4: Assumptions used for evaluating the
potential radiological consequences of a LOCA for PWRs

(2.1.1.1) Introduction

Section 50.34 of 10 CFR Part 50 (Ref. 1) requires that each applicant for a construction permit or operating license provide an analysis and evaluation of the design and performance of structures, systems, and components of the facility with the objective of assessing the risk to public health and safety resulting from operation of the facility. The design basis loss of coolant accident (LOCA) is one of the postulated accidents used to evaluate the adequacy of these structures, systems, and components with respect to public health and safety. Ref. 1 requires that the methods and assumptions described in two Regulatory Guides (1.3 and 1.4) should be used in this evaluation. In some cases, unusual site characteristics, plant design features, or other factors may require different assumptions which will be considered on an individual basis.

(2.1.1.2) Regulatory Position

Reg. Guide 1.3 and 1.4 require a demonstration that the offsite dose consequences will be within the guidelines of 10 CFR Part 100. (Ref. 4) The Reg. Guides require that during the construction permit review, lower guideline exposures, of 20 rem whole body and 150 rem thyroid, should be used rather than the values given in §100.11 of 10 CFR (Ref. 4) in order to allow for (a) uncertainties in final design details and meteorology or (b) new data and calculational techniques that might influence the final design of engineered safety features or the dose reduction factors allowed for these features.

For the convenience of readers, the assumptions related to the release of radioactive material from the fuel and containment which are specified in Reg. Guide 1.4 (Ref. 3) are reproduced in Appendix 1 to this report.

For evaluating the potential radiological consequence of a LOCA for boiling water reactors (BWR), Regulatory Guide 1.3 (Ref. 2) requires very similar assumptions to those of Regulatory Guide 1.4 (Ref. 3)

<2.1.2> Accident Assumptions for Environmental Reports

(2.1.2.1) Introduction

Prior to the issuance of a construction permit as an operating license for a nuclear power station, the Nuclear Regulatory Commission (NRC) is required to assess the potential environmental effects of that facility to ensure that issuance of the permit or license will be consistent with natural environmental goals which are expressed by the National Environmental Policy Act (NEPA) of 1969 (Public Law 91-190, P3 Stat. 852). In order to obtain information essential to this assessment, the NRC requires each applicant for a permit as a licensee to submit a report on the potential environmental impacts of the proposed station and associated facilities. The NRC's implementation of NEPA is contained in 10 CFR part 51, "Licensing and Regulatory Policy and Procedures for Environmental Protection." (Ref. 5)

The spectrum of accidents to be considered in preparing the section of Environmental Reports dealing with accidents is divided into eight classes, some of which have subclasses. It is stipulated that where assumptions are not specified, or where those specified are deemed unsuitable, assumptions as realistic as the state of knowledge permits

shall be used, taking into account the specific design and operational characteristic of the plant under consideration. For each class, except class 1 because of its trivial consequences, the environmental consequences should be evaluated as indicated.

Owing to their highly conservative nature, the assumptions and calculations used in NRC safety evaluations are not suitable for environmental risk evaluation, because their use would result in a substantial overestimate of the environmental risk. For this reason, class 2 to 8 accidents need to be evaluated realistically. Consequences predicted in this way will be far less severe for some events than those given, for example, in safety analysis reports, where more conservative assumptions are usually employed.

In this report, we have deemed it necessary to consider only class 8 events, which are design basis accidents that establish the performance requirements of engineered safety features, including the containment system.

2.1.2.2 Accident Assumptions

Since containment requirements are determined mainly by the potential consequences of class 8 accidents, it is of interest to compare the conditions postulated by NRC for PWR Environmental Reports with those postulated in the Regulatory Guides. The former are reproduced below. Those for accidents classes 2 through 7 are reproduced in Appendix 2.

Accident Class 8.0. Accident initiation events considered in design basis evaluation in the safety analysis report.

8.1 Loss-of-coolant accidents

Small Pipe Break (6 in. or less)

(a) Source term: the average radioactivity inventory in the primary coolant shall be assumed. (This inventory shall be based on operation with 0.5% failed fuel).

(b) Filter efficiencies shall be 95% for internal filters and 99% for external filters.

(c) 50% building mixing for boiling water reactors shall be assumed.

(d) For the effects of plateout, sprays, decontamination factor in pool, and core sprays, the following reduction factors shall be assumed:

For pressurized water reactors-0.05 with chemical additives in sprays, 0.2 for no chemical additives.

(e) A realistic building leak rate as a function of time shall be assumed.

(f) Meteorology assumptions: χ/Q values shall be 1/10 of those given in NRC Safety Guide No. 3 or 4.

(g) Consequences should be calculated by weighting the effects in different directions by the frequency the wind blows in each direction.

Large Pipe Break

(a) Source term: The average radioactivity inventory in the primary coolant shall be assumed. [This inventory shall be based on operation with 0.5% failed fuel], plus release into the coolant of:

For pressurized water reactors-2% of the core inventory of halogens and noble gases.

For boiling water reactors-0.2% of the core inventory of halogens and noble gases.

(b) Filter efficiencies shall be 95% for internal filters and 99% for external filters.

(c) 50% building mixing for boiling water reactors shall be assumed.

(d) For the effects of plateout, containment sprays, core sprays

(values based on 0.5% of halogens in organic form), the following reduction factors shall be assumed:

For pressurized water reactors-0.05 with chemical additives in sprays, 0.2 for no chemical additives.

(e) A realistic building leak rate as a function of time and including design leakage of steamline valves in BWRs shall be assumed.

(f) Meteorology assumptions: χ/Q values shall be 1/10 of those given in NRC Safety Guide No. 3 or 4.

(g) Consequences should be calculated by weighting the effects in different directions by the frequency the wind blows in each direction.

8.2(a) Rod ejection accident (pressurized water reactor)

(a) 0.2% of the core inventory of noble gases and halogens shall be assumed to be released into the primary coolant plus the average inventory in the primary coolant based on operation with 0.5% failed fuel.

(b) Loss-of-coolant accident occurs with break size equivalent to diameter of rod housing (see assumptions for Accident 8.1).

(c) The mechanical vacuum pump shall be assumed to be automatically isolated by high radiation signal on the steamline.

(d) Radioactivity shall be assumed to carry over to the condenser where 10% of the halogens shall be assumed to be available for leakage from the condenser to the environment at 0.5%/day for the course of the accident (24 hours).

(e) Meteorology assumptions: χ/Q values shall be 1/10 of those given in NRC Safety Guide No. 3.

(f) Consequences should be calculated by weighting the effects in different directions by the frequency the wind blows in each direction.

<2.2> Discussion of the Conservatism in NRC Requirements

(2.2.1) Introduction

In this Section the extent of the conservatism in the NRC Regulatory Guide 1.4 (Ref. 3) and in the "Requirements for Environmental Reports" (Reg Guide 4.2) is evaluated, on the basis that no accidents more severe than Class 8 (as defined in the previous section) has to be considered. This evaluation provides an indication of the additional, and possibly unnecessary, severity of the requirements imposed on PWR containment design by Reg. Guide 1.4 and by the "Requirements for Environmental Reports".

Hence it is appropriate to quantify the extent of the conservatism in the existing NRC requirements not only in relation to the source term (i.e., the fraction of the various isotopes released from the fuel) in accidents up to and including Class 8 accidents, but also in relation to the assumptions which have to be made concerning removal processes within the containment, leak rates, atmospheric dispersion and doses to the public. This quantification of the conservatism in the assumptions required by NRC can then be used to provide an indication of the maximum scope for reducing the performance required from the containment. The assessments of conservatism of the NRC assumptions have made in relation to the following areas:

- (a) source terms
- (b) leak rate and effect of containment pressure
- (c) isotope removal processes within the containment
- (d) atmospheric dispersion
- (e) calculation of dose due to external sources and to inhalation.

(2.2.2) Source terms

During the normal operation of light water reactors (LWR's) fission products are generated within the fuel elements. Most of these fission

products are radioactive and would be a hazard to public health if allowed to disperse in the environments. Releases of the gaseous and the more volatile radioactive isotopes, referred to as "source terms," can be postulated to occur as the result of a spectrum of potential accidents in LWRs.

In Reg. Guide 4.2 the postulated accidents are categorized according to "the class 1 through class 9" designations described in Appendix I of that document. They include eight accident types in class 3 through 8 for BWRs and ten types in classes 3 through 8 for PWRs. The design basis "loss of coolant accident" (LOCA) is one of the postulated severe accidents in classes 3 through 8 used to evaluate the adequacy of power plant structures, systems, and components with respect to the public health and safety (in Reg. Guide 4.2).

In Regulatory Guide 1.4⁽³⁾ the initiating event is not stated specifically but a set of assumptions are listed that should give an acceptably conservative source term. These assumptions are as follows:

(a) Twenty-five per cent of equilibrium radioactive iodine inventory developed from maximum full power operation of core should be assumed to be immediately available for leakage from the primary reactor containment. Ninety-one per cent of this twenty-five per cent is postulated to be in the form of elemental iodine, 5 per cent of this 25 per cent in the form of particulate iodine, and 4 per cent of this 25 per cent in the form of organic iodide.

(b) One hundred per cent of the equilibrium radioactive noble gas inventory developed from maximum full power operation of the core should be assumed to be immediately available for leakage from the reactor containment.

The realistic "source term," which can be used to prepare "Environmental Reports" by applicants for construction permits for PWR power plants, is described for LOCA in Appendix I of Reg. Guide 4.2 as follows:

- (a) The primary coolant activity is based on 0.5% failed fuel, plus the release of 2% of the core inventory of halogens and noble gases as a result of cladding failure during the LOCA. The cladding failure is assumed to occur after containment building isolation. The reactor is assumed to have operated at full power for 620 days prior to the accident.
- (b) All of the primary coolant is assumed to be released to the containment building.

During the last two decades knowledge of the basic physical processes concerned in fission product gas release from thermal reactor fuel has increased significantly. However, the exploitation of this knowledge to produce better practical methods of predicting fission gas release during accidents has not proceeded at a similar pace. There are two approaches to predict the fission product release during an accident; one is the use of analytical techniques which rely on the fully detailed physical model, the other is the use of a broad 'guide-line' type of correlation which provide interpretation of experimental release data for making gas release estimates. Detailed descriptions of the above approaches are given in Appendix 3.

In summary, the amounts of stable and long-lived isotopes that are released during the LWR Loss of Coolant Accident as calculated from each postulated model, assuming failure of all fuel rods but excluding the effects of previous fuel damage are compared in Table 2.1.

The comparison between the source terms of Reg. Guide 1.4, the Environmental Report Model and two more recent models, contained in Table 2.1, shows that the value of the Reg. Guide 1.4 model is one order of magnitude larger than those of the Environmental Report and WASH-1400 ⁽⁷⁾ models for iodine and nearly two orders of magnitude larger for noble gases. Another comparison of the source terms between the Reg. Guide 1.4 and the most recent ORNL model from Table 2.1 shows that the values of the Reg. Guide 1.4 ⁽³⁾ model are three orders of magnitude greater for noble gases and iodine than those of the ORNL model.

However, the validity of the ORNL model is questionable, particularly for transients in which the fuel is maintained at a high temperature for longer times than those used in the experiments on which the model is based (e.g. as in the case of the TMI 2 accident). Nevertheless, they do provide a further indication that the source terms proposed in the NRC's Regulatory Guides 1.3 and 1.4 are highly conservative for "well-conditioned" Class 8 accidents, as defined in the NRC's instructions for the preparation of Environmental Reports (Ref. 6), even if all the rods burst. Since the Standard Technical Specification for a PWR (Ref. 9) permits operation with up to 1.0 μCi I-131 per gm of coolant (corresponding to about 0.5 percent of the fuel being defective), it is necessary to consider whether the presence of this defective fuel can significantly increase the amount of activity released into the containment during accidents in Classes 3 through 8.

Two factors have to be considered:

(a) The amount of equilibrium activity in the coolant.

(b) Any additional release mechanism associated with the existence

*That is, an accident in which all the safety systems perform as expected.

of defects in the fuel existing prior to the accident.

It is shown in Appendix 3 that the contribution due to the first term is insignificant compared to that which would be expected from failure of the remaining rods in a LOCA e.g. At the limit imposed by the Standard Technical Specification, the amount of I^{131} in the coolant would be about 400 μCi as compared with the lower limit estimate of 3×10^4 Ci due to bursting of all rods.

However, it is necessary to consider also the effects of "iodine spiking". These are reviewed in Appendix 3 and it is shown that the somewhat limited data indicate that, for a 1100 MW(e) PWR, the additional contribution in a large LOCA could amount to about 1×10^4 $\text{Ci } I^{131}$ which is about 30 percent of the lower limit value estimated from the most recent ORNL model.

Thus, the total additional contribution is not negligible at the lower limit level, assuming that all rods fail. However, if the more conservative value of 1 percent release from the failed fuel were used, this additional contribution would be insignificant.

A further possible factor of conservatism in the source term for LOCA's is the proportion of rods which fail as a result of the accident. It can be claimed that, if all the safety systems function as designed, no additional rods would fail. Thus the most "realistic" source term would be the activity due to rods which had failed prior to the accident (See, for example Section 15.1.4 of the FSAR for the Virgil Summer NPP).

Prior to the realization of the importance of spiking, this implied that the release of I^{131} into the containment, for example, would be about 200 ci , or about 3×10^{-4} percent of the total inventory. However,

it follows from the preceding discussion that the contribution due to "spiking" could be of about the same order of magnitude as the lower limit for the "all rods failing" case, (i.e. 0.04 percent). Consequently, the source terms described as "realistic" in, for example, the FSAR referred to above are probably highly optimistic. Although it would probably be realistic to assume that not more than 10 percent of the rods fail in a "well-conditioned," large LOCA, nevertheless this "realistic" source term would still be an order of magnitude too small.

In the case of a BWR, the additional contribution due to "spiking" appears to be a factor of 10 smaller than in PWR (See Appendix 3) and is therefore insignificant.

Table 2.1. Fission Product Release in an LWR Loss of Coolant Accident, Excluding Effects of Previously Damaged Fuel

Element	Release as Percent of Total Inventory			
	Reg. Guides 1.3 & 1.4 Models	Environmental Report Model	WASH-1400 Model	ORNL Model ^(a) (Lorentz et al)
Kr & Xe	100	2.	3	0.9
Cs	—	—	5	0.07
I	25	2.	1.7	0.04

Notes: (a) These data have been modified from the original data for our own purpose, based on their assumptions⁽³⁾ as follows;

(1) The gap inventory and total release of ¹³¹I and ¹³³Xe would be 3 to 9 times lower.

(2) An additional amount of fission gas, approximately 1.5(%) of the total inventory, would be released during heatup.

(b) In the case of a PWR the presence of previously damaged fuel is

estimated to increase the amounts of Cs and I by about 30 percent (i.e. to 0.07 percent of Cs inventory and 0.04 percent of I inventory), if the reactor is operating within the levels imposed by the Standard Technical Specification. In the case of a BWR the corrections would be insignificant.

(2.2.3) Containment leak rates

Once the pressure transients associated with design basis accidents (DBA's) have been determined, containment strength requirements can be set so that structural failure leading to the release of containment atmosphere to the environment will not occur. However, because of inevitable structural imperfections and defects in seals, the containment will not have a zero leak rate. As the leak rate will vary with pressure, this maximum rate must be related to some specific pressure. A value for the maximum containment leak rate for a light water reactor, at the peak calculated internal containment pressure, is incorporated as a Technical Specification requirement. A typical value is 0.2(%) volume of containment atmosphere per day. Reg. Guide 1.4 (Ref. 3) requires that this leak rate should be assumed to persist for the first 24 hours and that leakage should continue at 50 percent of this rate for the remaining duration of the accident.

For the accidents defined in the Environmental Report Model, it appears at first sight that it would be unduly conservative to assume that the leakage rate remains constant at the design value for the first 24 hours, since the primary containment pressure would decrease after the first few minutes. However, the relationship between pressure and leak rate is very dependent on the type of flow through the leaks. A detailed evaluation of the relationship between pressure and leak rate for various possible flow regimes, is given by Appendix 4 to this report.

It is shown that, of the four flow regimes considered [orifice flow, turbulent flow (rough passage), turbulent flow (smooth passage) and laminar flow], the leak rate is more sensitive to pressure in the case of laminar flow and least sensitive for orifice flow. In practice there is no means of determining, prior to construction, what proportion of the leakage will be attributable to each type of flow. Consequently it has to be assumed in design that the relationship between leak rate and pressure will be the least favorable (i.e. orifice flow). It will be seen from Fig. 4.7 of Appendix 4 that, for a typical PWR "dry" containment, the leak rate due to orifice flow would be reduced by 50 percent when the absolute pressure has fallen to 1.1 atm. from an initial value of about 5 atm; whereas, if the flow were entirely laminar it would have fallen to 50 percent at about 2.7 atm.

Using the data given by Bechtel in Ref. 10 for the pressure/time relationship, for a typical PWR "dry" containment, in conjunction with the results obtained in Appendix 4, it is found that (with the unavoidable assumption that all the leakage is due to orifice flow) the average leak rate over the first 24 hours is 84.3 percent of the initial value; the rate at 24 hours is about 75 percent of the initial value, i.e. the assumptions required by Reg. Guide 1.4, in this instance, do not necessarily contain any conservatism. However, testing of a containment, as built, at design pressure and at some fraction of design pressure would provide a more representative indication of the actual pressure/leak rate relationship and might well reveal some measure of conservatism in the assumptions, for that structure.

(2.2.4) Isotope removal process

There are basically two concepts of fission product removal used in containment system design; they are removal within the primary containment structure boundary and removal outside of that boundary, but within a secondary containment boundary.

For those containment systems using internal fission product removal mechanisms, the following mechanisms have been employed:

- (a) removal by air recirculation through charcoal filter beds.
- (b) removal through the containment spray process.
- (c) removal through natural deposition ("plate out") on containment surfaces

The charcoal filter removal system was used fairly extensively in the 1966 to 1970 vintage PWR plants. This system made use of the existing containment fan coolers, and simply added a HEPA/charcoal adsorber filter train in series. This method relies on fan forced recirculation of the virtually 100% humidity post-accident containment atmosphere through the adsorber beds to remove radioactive halogens, particularly iodine. The removal rate is dependent on the rate of containment air recirculation through the filters, as well as the filter removal efficiency. This results in the following requirements⁽¹¹⁾ :

- (a) very large fan motors qualified to operate in the post-accident environment conditions of temperature, pressure, and humidity;
- (b) large additions to diesel generator capacity requirements;
- (c) seismic category I containment duct systems to assure fairly uniform sweeping of the containment atmosphere; and
- (d) charcoal filter material with high filter efficiencies at high humidity for both organic and inorganic forms of iodine.

Despite development efforts, charcoal filter efficiencies for elemental iodine drop off dramatically at 100% humidity conditions, and at lower humidities for organic iodines, especially if there is a possibility of filter water logging. These factors make this method of fission product removal very expensive and the last choice of most designers.

A more popular method of internal fission product removal from PWR's uses the containment spray system (CSS). A very fine mist is produced by sprays, which tend to adsorb both airborne particulate and gaseous iodine. The extent of adsorption depends upon droplet size, free fall height, and pH. Solution pH also affects the capability to retain the adsorbed iodine in solution in the containment sump.

The water used in PWR ECCS has to be borated to maintain reactivity shutdown margin in the reactor core. Borating increases iodine adsorption to some extent but the addition of either sodium hydroxide or sodium thiosulfate has been found to greatly enhance the iodine-getter properties of the spray systems, with the hydroxide giving a somewhat higher removal rate. For economy, it is convenient to use the same stored water for refueling operation and for emergency core cooling system (ECCS) and containment spray functions, but for the refuelling role hydroxide additions would be unacceptable. Thus if an hydroxide additive is used as an iodine getter, it must be stored in a separate container and mixed with the spray solution when it is called upon for LOCA conditions.

The mechanism of natural deposition of particulate is dependent mainly on impaction processes, since the containment atmosphere circulates as a result of natural convection generated by temperature differences between bulk gas and containment wall. In the case of gaseous halogens,

the mechanism of plateout is surface adsorption. Several models have been proposed to describe fission product removal for particulates and halogen gases due to surface deposition and adsorption.

The more detailed theoretical and experimental work on models of spray removal and natural deposition are described in Appendix 5.

Fission product removal systems external to the primary pressure containment boundary generally consist of a secondary containment structure enclosing the primary one. This kind of system is usually referred to as a "double barrier" PWR containment. Leakage from the primary boundary is permitted at a low rate, of the order of 1% per day. The secondary volume is maintained at a slightly negative pressure with respect to the outer atmosphere through operation of a fan system. This fan system exhausts to the atmosphere via a filter system.

As far as isotope removal process is concerned, the Reg. Guide⁽³⁾ 1.4 does allow case-by-case evaluation of the removal factor so that, in principle, this may not be treated very conservatively. From various FSAR and NRC Safety Evaluation Reports, it appears that both the NRC and applicants have used a value for the removal factor which was established several years ago. If the NRC were to choose the more recent realistic spray removal coefficient value⁽¹²⁾ ($\lambda=17.0 \text{ hr}^{-1}$), which is in good agreement with the Containment System Experiment (CSE) tests instead of the old Stagnant Film model value ($\lambda=10\sim12.55 \text{ hr}^{-1}$), then the spray removal for elemental iodine would be calculated to be 35~70 percent more efficient than before. Thus there is, at present, a factor of conservatism of about 1.5 in the evaluation of the spray systems.

(2.2.5) Atmospheric dispersion

In order to assess the radiological implication of radioactive material release, it is necessary to determine the distribution of radioactive species released to the atmosphere and their subsequent transfer to biological system. For a fixed release quantity, the magnitude and duration of exposure to a given dose level, depends upon the characteristic of the transport medium (i.e., the atmosphere) during the time of release.

An analytical method based on a generalized three dimensional, time-dependent diffusion equation including sources can be used to describe the turbulent diffusion of radioactive pollutants released to the atmosphere. A detailed discussion of this method is given in Appendix 6. However, most workers in the area of atmospheric dispersion prefer to make use of the method devised by Taylor,⁽¹³⁾ which is based on statistical theory. In this method, the concentration, X , of gas or aerosols (less than about 20 micron diameter) at x, y, z from a continuous source which has an effective emission height, H , is given by Equation (1) as follows:

$$X(x, y, z, H) = \frac{Q}{2 \pi \sigma_y \sigma_z \bar{u}} \exp\left[-\frac{1}{2}\left(\frac{y}{\sigma_y}\right)^2\right] \left\{ \exp\left[-\frac{1}{2}\left(\frac{z-H}{\sigma_z}\right)^2\right] + \exp\left[-\frac{1}{2}\left(\frac{z+H}{\sigma_z}\right)^2\right] \right\} \quad (1)$$

where X = concentration of a given radioactive material at position (x, y, z) in Curies/ m^3 ,

Q = magnitude of the instantaneous release of radioactive material in curies/sec,

$\bar{u}$ = mean wind speed affecting a plume in m/sec,

σ_y, σ_z = standard deviations of plume concentration distribution in the horizontal and vertical direction respectively in meter².

The main assumption for Equation (1) are;

- (1) the plume spread has a Gaussian distribution in both horizontal and vertical planes,
- (2) total reflection of plume takes place at the earth's surface, i.e., there is no deposition or reaction at surface,
- (3) the diffusion of the plume travel is assumed to be neglected, i.e., no diffusion in x-direction.

If the receptor is located at the ground level ($Z=0$), then

$$X/Q = \frac{1}{\pi \sigma_y \sigma_z \bar{u}} \exp \left[-\left(\frac{y^2}{2\sigma_y^2} + \frac{H^2}{2\sigma_z^2} \right) \right] \quad (2)$$

which is the form of the Gaussian plume model most usually used.

The Regulatory Guide 1.4 for evaluating dispersion for class 3 through 8 accident used Equation (1) with the following assumptions: concentrations are calculated at ground level, i.e., $Z=0$, along the centerline plume ($y=0$), and for a ground-level source with no effective plume rise ($H=0$).

Equation (1) then takes the simpler form

$$X(x,0,0,0) = \frac{Q}{\pi \sigma_y \sigma_z \bar{u}} \quad (3)$$

For time period of greater than 8 hours, the plume is assumed to meander and spread uniformly over a 22.5° sector. The resultant equation is

$$x(x,0,0,0) = \frac{2.032 Q}{\sigma_z \bar{u} x} \quad (4)$$

where x = distance from point of release of the receptor.

Material released from building vents, from short stacks or at ground level may be subject to enhanced dispersion due to the turbulent

wake caused by air flow around the building. Building wake corrections are generally considered only for ground level release.

Regulatory Guides 1.4 suggests use of the building wake correction model presented by Slade⁽¹⁴⁾. This model is to be applied only to ground level releases of eight hours or less duration. The dispersion correction factor is limited to the range of 1/3 to 1. The Gaussian dispersion equation for a normalized concentration at ground level with building wake correction becomes

$$X/Q = \frac{1}{\pi \Sigma_y \Sigma_z \bar{u}} \quad (4)$$

where $\Sigma_y = (\sigma_y^2 + 0.5A/\bar{u})^{1/2}$

$\Sigma_z = (\sigma_z^2 + 0.5A/\bar{u})^{1/2}$

A = the minimum cross sectional area of reactor building (M^2)

These equations are believed to give "best estimate" values of the concentration at this axis, i.e. the actual concentration is as likely to be larger as smaller than the estimated value. However, NRC has introduced some conservatism in their assumptions concerning the atmosphere stability conditions assumed to exist during the post accident period. In Reg. Guides 1.3 and 1.4 it is assumed conservatively that Category F weather conditions are present for the whole of the first 24 hours and for 66 per cent of the time in the next 3 days. An indication of the effect of this assumption on the value of the diffusion factor is provided by Fig. 2.1. This shows that the assumption of 100 per cent persistence of Category F would lead to a 3 fold increase in the dilution factor, as compared with 30 percent persistence. The factor of conservatism has been estimated for an average U.S. site by evaluating the weighted mean diffusion factor for the average meteorological conditions

(taken over 33 sites) which are given in Table VI-3 of Ref. 15. This method indicates that the factor of conservatism in the diffusion factor computed according to Reg. Guide 1.4 is about 3.

The Environmental Report Model allows the use of dilution factors which are ten times as large as those used in the Reg. Guide 1.3 and 1.4 (See Sec. 2.1.2.2, above). Consequently, in terms of realistic values for the average US sites considered above, the Environmental Report Model is non-conservative by a factor of about 3. Thus the predicted doses (given the specified source terms) are higher by a factor of 3 than can readily be justified.

One omission from Regulatory Guide 1.4 relates to deposition of active isotopes from the plume. The Guide does not require this potential source in radiation exposure to be considered. However, for completeness the appropriate choice of the main parameter, the deposition velocity is discussed in Appendix 3.

Fig. 2.1 Variation of Average Dilution with Fraction of time for which highly stable conditions persist, as compared with category 'D' conditions.

<2.2.6> Dose Calculations

<2.2.6.1> Introduction

Offsite radiological analysis required for an accidental release of radioactivity to the environment consists of determining the inhalation and external whole body doses resulting from exposure to the airborne radioactive materials. The methods for calculating the inhalation and whole body dose given in Reg. Guide 1.4⁽³⁾ are based mainly on the recommendations of the International Commission on Radiological Protection (ICRP), but some unduly conservative assumptions are made; the overall effect of these is discussed below.

2.2.6.2 External Whole Body Dose

Reg. Guide 1.4 requires that these should be calculated on the basis that the cloud is of semi-infinite extent. This is unnecessarily conservative, particularly for points relatively close to the source (e.g. at the exclusive area boundary (EAB)) and for very stable conditions.

The extent of the conservatism arising from this assumption can be estimated from the results given by Beattie and Bryant in Ref. 1B. (Their Fig. 6). Assuming, as in the previous section, that highly stable conditions persist for 50 percent of the time, it is found that there are factors of conservatism of 3 at 1 km and 2 at 5 km.

<2.2.6.3> Dose to thyroid from inhalation of iodine

Reg. Guide 1.4 requires that a breathing rate approximately 50 per cent higher than normal should be used for the first 8 hours after the release. This seems to be unnecessarily conservative for members of the public.

Reg. Guide 1.4 specifies that the factors for converting the intake of active iodine to thyroid dose should be based on those given in "ICRP Publication 2" (Ref. 16). However, these values were calculated some 20 years ago. More recent calculations, using more refined models show a considerable variation from the ICRP values, as shown in Table 2.3. It will be seen from the Table 2.3 that for the two important isotopes (I^{131} and I^{133}) the use of the ICRP Publication 2 values over-estimates the dose per curie inhaled by a factor of at least 1.5. Combined with the conservatism in the breathing rate for the first 8 hours this leads to a factor of conservatism of about 2.

Table 2.2 Breathing Rates Specified in Reg. Guide 1.4⁽³⁾

Time after accident	m ³ /sec.
0 to 8 hours	3.47×10^{-4}
8 to 24 hours	1.75×10^{-4}
1 to 30 days	2.32×10^{-4}

Table 2.3 Iodine Dose Conversion Factors

Isotopes	Rem-thyroid/curies inhaled	
	Reg. Guide 1.4 ⁽³⁾	WASH-1400 ⁽¹⁷⁾
I-131	1.48×10^6	1.1×10^6
I-132	5.35×10^4	6.6×10^3
I-133	4.00×10^5	1.8×10^5
I-134	2.50×10^4	1.1×10^3
I-135	1.25×10^5	4.4×10^4

<2.2.7> Extent of conservatism in 10CFR 100 guide-lines for radiological consequences

<2.2.7.1> Cancer risk from exposure to the "guide-line" consequences

It is usually accepted that the chance of a fatal, delayed, cancer developing during a life-time, due to whole body exposure to radiation is about 1×10^{-4} per rem. The corresponding chance for thyroid cancers of any sort is about 0.1×10^{-4} per rem, due to inhalation of active iodine (App. 6 of Ref.⁷). However, only a small percentage of these should prove fatal; a conservative estimate of the proportion of fatal thyroid cancers, given adequate medical surveillance and care, is 10 percent. The probability of a fatal, delayed thyroid cancer due to external radiation is approximately 10 times greater than for the case of I^{131} inhalation. Thus, the overall probability of a fatal cancer from exposure to the 10 CFR 100 "guide line" consequences (i.e. 25 rem whole body and 300 rem to the thyroid due to iodine inhalation) is about 3×10^{-3} , if both components have their maximum value.

In principle a somewhat higher value should be taken to allow for the effects of other volatile isotopes, notably C_s^{137} , which would be expected to accompany the iodines. The correction can be estimated as follows: assuming that the proportion of C_s^{137} released into the containment is twice that of I^{131} (App. 7 of Ref. 7) and that the subsequent removal processes are equally efficient for both, it is found that the probability of fatal delayed cancers due to C_s^{137} would be approximately 30 percent of that due to I^{131} . Thus the additional contribution to the probability of total delayed cancers, due to C_s^{137} , is less than 5 percent of the total and can be neglected.

From an economic point of view the probability of an accident leading to severe damage to the fuel would need to be kept below 10^{-4} per year, owing to the heavy cost of the prolonged outage of a nuclear power plant (e.g. see Ref. 19). Thus, the combined probability of the event visualized in Reg. Guide 1.4 occurring and of any one individual within the LPZ (low population zone) developing a fatal cancer later in life, as a direct result, is conservatively estimated to be less than 3×10^{-7} per year. This is substantially lower than the probability of immediate death from accidents due to causes that are virtually unavoidable in U.S. society (about 1×10^{-4} per year, Ref. 20). Thus, on a simple "comparability of risk" basis the "guide line" consequences could be regarded as reasonably conservative. However, as discussed in Sec. 3 below, from some points of view, they might not be regarded as sufficiently restrictive.

It should be noted that if we were concerned only with accidents in Classes 3 through 8, as defined in Sec. 2.1.2, above, the actual maximum radiation exposures should be lower than the "guide line" values, due to the smaller source terms. Taking account of the reduction in source terms only, the probability of delayed fatal cancers due to the most severe Class 8 accident would be reduced by a factor of at least 30 (i.e. to less than 1×10^{-8} per year). It can be shown, as in the next Section, that the elimination of other factors of conservatism in Reg. Guides 1.3 and 1.4 would reduce this probability by another order of magnitude.

2.3 The extent of the conservatism in NRC requirements

(2.3.1) Dry PWR Containment

(2.3.1.1) Introduction

Since the NRC requirements determine the performance and design parameters for the containment system, the various factors of conservatism identified in the preceding Sections are summarized below. For completeness, the factors of conservatism, both in Reg. Guides 1.3 and 1.4 and in the Environmental Report Model,⁽⁶⁾ have been summarized.

There is a possibility that Reg. Guides 1.3 and 1.4 might be revised in the future; the available information as to the nature of the changes is discussed in Sec. 2.3.1.3, below.

2.3.1.2 Summary of factors of conservatism in Reg. Guides 1.3 and 1.4.

The various factors of conservatism in the approach adopted in Reg. Guide 1.4 and in the Environmental Report Model which have been discussed in the preceding Sections are summarized in Tables 2.4(a) and 2.4(b). This summary is based on the dominant accident in Class 8, namely the large LOCA.

For ease of comparison, the effects of the conservatism in the estimates of whole body dose and thyroid dose have been combined by estimating the probability of an individual developing a delayed fatal cancer as a result of the joint effects of the doses which have been estimated using the assumptions specified in the Reg. Guides and those which would be received if these were reduced by the factors of conservatism.

In view of the degree of uncertainty about the source terms the comparisons have been made for two sets of assumptions:

- (a) That the releases of rare gases and iodines into the con-

tainment are in accordance with the estimates given in the Environmental Report Model (i.e. 2 per cent for rare gases and 2 per cent for iodine); assuming failure of all of the rods.

(b) That the releases are in accordance with the latest ORNL model (assuming failure of all of the rods), corrected for the effects of "iodine spiking" as discussed in Sec. 2.2.2.

The effect of assuming that only 10 per cent of the rods fail in the LOCA has also been examined. Table 2.5 contains the results of this comparison.

Table 2.5 Evaluation of conservatism in Reg. Guide 1.4 in terms of reduction in probability of delayed fatal cancers.

Source term used in comparison	Apparent reduction in probability of delayed fatal cancer
Environmental Report Model, 100 per cent rod failure	300
Environmental Report Model, 10 per cent rod failure	3000
ORNL model, plus spiking, 100 per cent rod failure	1000
ORNL model, plus spiking, 10 per cent rod failure	7000

Thus, providing that it were unnecessary to consider accidents worse than a "well-conditioned" large LOCA (i.e. one in which the protection system behaved in accordance with design) it is possible that the performance requirements for the containment of a large PWR could be reduced by factors of at least 100, without actually exceeding the radiological consequences specified in Reg. Guide 1.4. Thus it could be argued that a much lower standard of performance should be sufficient. The minimum standard required is discussed in Sec. 4.2.1 below.

Subject to the same provision, a similar relaxation in the containment requirements for a large BWR should also be possible.

However, in practice, relaxations of this magnitude may not be

possible on account of:

- (a) The unexpectedly high release of fission products from the fuel in the Three Mile Island reactor accident, even though the fuel did not melt.
- (b) There are other arguments, which are discussed in the next Section, that suggest it might be desirable to retain the current standards of performance, or even to improve upon them.

Consequently the re-optimization of containment-design has had to be considered on a wide basis (See Section 6, below).

(2.3.1.3) Possible Revisions of NRC Requirements

The revision of Reg. Guide 1.3 and 1.4 is currently under review by NRC. (Ref. 21). One form of revision, which has been suggested by the Director, Office of Nuclear Reactor Regulation (NRR) would increase the present source terms, by changing the amount of radioactive iodine available for release from the primary containment to 50 percent of the inventory, as compared with the present value of 25 percent, and in addition one percent of other non-gaseous fission products would be assumed to be released into the primary coolant. However, credit would still be given for reduction in the iodine concentration by plateout, using suitably conservative estimates and credit could be taken for reduction of containment pressure 2 hours after the start of the release, instead of 24 hours later.

In general, it seems that a more flexible attitude towards the use of engineered safeguards to reduce the iodine concentration is visualized but the reliability of such systems would be subject to the usual NRC requirements concerning the incidence of single failures.

For PWRs with conventional 'dry' containments the other requirements of the proposed draft Reg. Guides would be similar to those in

the existing one. In addition, specific proposals are made for the treatment of secondary containments, where these are provided.

A further conservatism in the proposed Reg. Guide is that, at the application for a construction permit stage, significant uncertainties in the estimated doses should be taken into account by an appropriate reduction in the 10 CFR 100 values.

Overall, it appears that the draft proposals put forward by DONRR would be somewhat less conservative than the existing Reg. Guide but nevertheless the fundamental difficulty remains, namely that the source terms correspond to accidents more severe than any of those defined in classes 3 through 8 in Ref. 3. However, in view of the large fractions of gaseous and volatile products released in the TMI accident, without meltdown of the fuel (i.e. short of a Class 9 accident) it could be argued that these large source terms are not unduly conservative. This aspect will be the subject of further study by UCLA.

Table 2.4 Factors of conservatism in NRC Reg. Guides 1.4 and 4.2, for dominant Class 8 Accident (Large LOCA)

Table 2.4(a) Parameters other than the source terms

Basis of Comparison	Plate-out Spray etc. for volatiles	Integrated leakage [§]	Atmospheric Dispersion	Dose conversion factor	Overall factor Rare Gases	Volatiles
Reg. Guide 1.4 with "best estimate" value	1.5	1.0	3.0	2.0	6.0	9.0
Environmental Report Model with "best estimate" value	1.5	1.0	0.3	2.0	0.6	0.9

§ For conventional single shell, "dry" containments with low-leak rate steel liners or vessels.

Table 2.4(b) Comparison including source terms

Basis of Comparison	Source term used for comparison	Apparent factor of conservatism in source term		Overall factor of conservatism	
		Rare Gases	Volatiles	Rare Gases	Volatiles
Reg. Guide 1.4 with "best estimate" values	Environmental Report Model, 100% pin failures	50	12.5	300	110
	Environmental Report Model, 10% pin failures	500	120	3000	1100
	ORNL Model, 100% pin failures	100	600	600	5400
	ORNL Model, 10% pin failures	1000	1600	6000	14,000
Environmental Report Model with "best estimate" values	Environmental Report Model Source (100% pin failures)	1.0	1.0	0.6	0.9

Table 2.4(a) continued

Basis of Comparison	Source term used for comparison	Apparent factor of conservatism in source term		Overall factor of conservatism	
		Rare Gases	Volatiles	Rare Gases	Volatiles
Environmental Report Model with "best estimate" values	ORNL Model 100% pin failures	2.2	50	1.3	45
	ORNL Model 10% pin failures	22	500	13	450

References of Section 2

- (1) 10 CFR Part 50, "Licensing of production and utilization facilities."
- (2) U.S. Nuclear Regulatory Guide 1.3, "Assumptions used for evaluating the potential radiological consequences of a loss of coolant accident for boiling water reactors," Revision 2, 1974.
- (3) U.S. Nuclear Regulatory Guide 1.4, "Assumptions used for evaluating the potential radiological consequences of a loss of coolant accident for pressurized water reactors," Revision 2, 1974.
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3. Definition of possible targets for containment performance

(3.1) The nature of the problem

Containment performance depends partly on the leak rate of the containment structure, at its design pressure, and partly on the effectiveness of the system provided for functions such as decreasing the containment pressure (in order to reduce the leak rate) and removing volatile fission products from the containment atmosphere. There is some scope for design optimization since, for example, improvement of the efficiency of a spray system, to reduce the iodine vapor concentration, may be considerably cheaper than attempting to reduce the design leak rate of the structure as a means of achieving the same overall effectiveness.

In the past, the NRC requirements as stated in Reg. Guide 1.3 and 1.4 have provided an adequate basis for design optimization, within the narrow limits which are feasible for such seemingly conservative requirements, in an era when no specific reliability requirements had to be met.

In the future it is possible that a more "risk-oriented" approach to design might be required in which the performance and reliability of the reactor protection systems, combined with that of the containment system, would be required to give either a constant or diminishing level of risk (i.e., probability x consequence) as the potential consequences of an accident increased.

It may be noted that an approach of this nature has recently been adopted in the United Kingdom; the latest draft of the "Safety Principles" document (Ref. 1), which the Chief Inspector of Nuclear Installation (CINI) is about to issue for public comment, contains the following proposals:

- (i) If the probability of an accident is greater than 0.03 per reactor year (i.e. about once per reactor life time), the dose to a member of public should not exceed 1/30 of the annual dose limit for the general public. (i.e. about 17 m rem)
- (ii) If the probability of an accident is greater than 3×10^{-4} per reactor year (i.e. about once in 30 years for a national program of 100 reactors), the dose to a member of the public should not exceed the annual dose limit. (i.e. 0.5 rem)
- (iii) If the consequences of a more severe accident would give to a member of the public a dose greater than an Emergency Reference Level* (ERL) dose, the probability of such an accident must be made as "low as reasonably achievable."

The combined effect of proposals (i) and (ii) should be to avoid any accident exposure of the public greater than the limits which are appropriate to normal operation, during the reactor program; the effect

*An ERL is defined as that level of dose from an accident above which counter-measures (e.g. evacuation) should be undertaken, if by those means a substantial reduction in the potential dose is likely to be achieved. The various ERLs, which are specified by the U.K. are specified by the U.K. Medical Council, are approximately twice the occupational dose levels to the same organ, e.g. the ERL for whole body doses 12 rem.

of (iii) should be to reduce the risk from the more severe accidents to a level considerably lower than that which the UK Health and Safety Executive (to whom the CINI is responsible) would consider acceptable for other potentially hazardous industries.

The Three Mile Island accident has demonstrated that the U.S. public is extremely sensitive to nuclear accidents which appear to present the slightest hazard to their health. Thus an approach similar to that adopted by the U.K. regulatory body might be attractive to U.S. utilities as a basis for a design specification, since this means it should be able to maintain a very low "safety profile" throughout the thermal reactor program, except in the event of severe but unlikely accidents leading to consequences greater than those expected in accident classes 3 through 8 of Reg. Guide 4.2. It is convenient to define such a combination of reactor and containment as a "No significant harm reactor".

Thus it should be a useful exercise to determine whether the containment requirements to meet a set of "No significant harm" criteria, such as those described above, for accidents in classes 3 through 8 would be substantially less onerous than those required to meet existing, or anticipated, NRC requirements. If the requirements were substantially less onerous it would then be worth while to determine whether any significant savings in cost could be made by modifications to current design specifications (e.g. by relaxation of the leak rate requirement and a reduction of the high quality currently needed for the containment liner), possibly by the greater use of engineered safety systems.

Given that a worthwhile saving in cost appeared to be feasible it

would then be necessary to determine the reliability which could be expected from the simplest version of the revised system and to consider whether it would be adequate. If not, the increased plant requirements would, of course, reduce the expected cost savings.

The next step, to provide a basis for negotiation with NRC, would then be to determine how large a source term could be accepted, or how much accident severity could extend beyond class 8, before the limits of 10 CFR 100 were exceeded.

It should be noted that, if the concept of a "no significant harm" containment design were adopted, it would also be necessary to consider the extent to which the "uncontained accidents" in classes 3 to 8 (e.g. fuel handling accidents, radwaste accidents and failure of steam pipes outside the containment) would necessitate additional protection, first-ly on simple deterministic grounds and secondly on the grounds of comparative risk. However, this aspect of the problem has been considered to be outside the scope of the present study.

(3.2) Definition of assumptions for a "no significant harm" reactor

For the purpose of defining the reactor containment specification for a "no significant harm" response, if the reactor were subject only to accidents in classes 3 through 8, the assumptions defined below have been made initially; the sensitivity of the containment requirements to these assumptions is explored later. The initial assumptions are as follows:

(a) Source terms

The source terms are those specified originally in the Annex to Appendix D of 10CFR 50 (Ref. 2), except that equal fractional releases

of cesium isotopes have been assumed to be released in all situations where specific reference is made to iodine releases, and the "spiking" effect on iodine and cesium isotopes releases have been taken into account.

(b) Atmospheric dispersion

The dispersion conditions assumed are those for the average of 33 U.S. sites (as discussed in Sec. 2.2.5, above) which leads to a three fold reduction in the diffusion factor as compared with Reg. Guide 1.4.

(c) Acceptable limits on dose

For a "no significant harm" situation some specific limits on dose have been assumed. These limits, which are based on the radiation exposure at the EAB, and the reasons for their selection, are as follows:

- (i) Whole body γ -dose incurred during the first 48 hrs of the release, at EAB-30 mrem (Note). This dose is assumed to be made up of components due to:
 - . external dose from rare gases in cloud
 - . external dose from other isotopes in cloud
 - . external dose from deposited material
 - . internal dose from inhaled material
- (ii) Whole body γ -dose accumulated in first year at EAB-50 mrem (Note) This dose is assumed to be made up of components due to:
 - . dose in first 48 hours, as in (i)
 - . additional dose from deposited material (mainly Cs137) i.e., this component is limited to 20 mrem
- (iii) Dose to child's thyroid incurred during the first 48 hrs of the release, at EAB-0.5 rem.

(Note) This dose is assumed to be made up of components due to:

. inhaled isotopes of iodine	}	with appropriate factors
. external γ -radiation		(as in appendix VI/H of WASH-1400)

(IV) Dose to child's thyroid due to consumption of contaminated milk

- 0.5 rem.

(Note) This dose is assumed to be due to be entirely to iodine deposited on herbage. It is assumed that the active iodine content of milk consumed by any one child would represent the average contamination in a sector 1 Km wide, extending downwind from the EAB, in the most heavily contaminated direction.

The bases for the limits suggested above are as follows:

- . The radiation exposure of individuals at the EAB who are NOT evacuated is proposed as a basis for a "no significant harm" design, since any need for evacuation could give rise to widespread anxiety and adverse effects on public opinion.

- . Whole body γ -dose incurred during the release (30 mrem).

This is 1/17th of the maximum dose allowed for the general public in normal operation of the plant; any contained accident likely to give rise to a dose of this size would almost certainly necessitate an outage of more than 2 months (i.e. 1/6th year) for repairs.

Thus the reduction in dose during the enforced outage should offset the dose received as a result of the accident.

- . Whole body γ -dose accumulated in first year. The incremental dose due to deposited material in the first year would continue at a reduced level in the second and subsequent years but, due to weathering, would probably not exceed 10 mrem, i.e., there would be no significant addition to natural background, due to the

deposited material or to the nuclear power plant itself, if the reactor were restarted.

- . Dose to child's thyroid; In accidents where thyroid effects are likely to be limiting, the dominant contribution to the dose would be inhaled iodine. Taking a ratio of 10:1 for the thyroid cancer risk from external γ -radiation as compared with iodine deposited in the thyroid (Ref. 4), a thyroid dose of about 500 mrem would be equivalent to 50 mrem whole body in terms of risk of cancer.
- . Thyroid dose from milk; so few cows could be grazing at the EAB that it would be unrealistic to take the herbage dose at the EAB as the limiting figure. The average value for a sector between EAB and (EAB + 1 Km) is considered to be a more realistic basis for the degree of contamination.

(d) Site condition

The EAB is assumed to be at a distance of 1 Km from the reactor.

The wind directions are assumed to be uniformly distributed.

3.3 Comparison of containment requirements to meet NRC and "No significant harm" criteria

3.3.1 Method of Comparison

The containment requirements to meet the NRC exposure limits defined in Reg. Guides 1.3 and 1.4 and those to meet the "no significant harm criteria" defined in the previous Section can be compared initially in a simple, approximate way by consideration of the whole body doses received at the EAB due to external radiation from the rare gases and volatile fission products and the thyroid doses due to iodine. A more exact comparison can be made by comparing the probabilities of fatal delayed cancers in the two cases, taking into account the other possible components discussed in the previous Section.

3.3.2 Quantitative comparison of requirements

Table 3.1 provides a comparison of the whole body doses due to external radiation (i.e., to the rare gases and volatile fission products) and of the thyroid doses due to iodine, at the EAB, which would be acceptable in terms of Reg. Guides 1.3 and 1.4 and in terms of the "no significant hazard" requirement discussed in Section 3.2. In the latter case the doses due to the external radiation and to the iodines should be reduced to some extent, in order to allow for the other possible contributions to the health hazards.

Table 3.1 Approximate quantitative comparison of requirements imposed by the criteria of Reg. Guides 1.3 and 1.4 and by the "No significant harm" (NSH) concept.

Source of hazard	Type of Criterion		Ratio $\left(\frac{\text{"Reg. Guide"}^1}{\text{NSH}}\right)$
	Reg. Guides	"No significant harm"	
Whole body dose (external radiation)	25 rem	50 mrem	500
Thyroid dose	300 rem	500 mrem	600

It has been shown in Section 2 that the overall factor of conservatism in the evaluation of the hazard in Reg. Guides 1.3 and 1.4, as compared with a "best estimate" evaluation, is about 600 for the whole body dose (if this is attributed to rare gases alone) and about 5,000 for the thyroid dose due to iodine, if, in the best estimate evaluation, the most favorable interpretation of the recent experimental data on gas releases from ORNL is employed. (See Table 2.4) However, if the more conservative data on gas releases, which are summarized in WASH 1400 (Ref. 3), are used, these factors of conservatism are reduced to 200 and 130 respectively. The degree of conservatism has also been assessed in terms of the probability of fatal delayed cancers by taking into account also the external doses to thyroid and whole

body due to the volatile fission products. On this basis the factors of conservatism for these 2 sets of estimates are 1000 and 190 respectively. It must be remembered that the "no significant harm" criteria are based on continuous exposure at the EAB for 48 hours, whereas the Reg. Guide criteria assume only 2 hours exposure. With this reservation, it will be seen from Table 3.1 that even on a "best estimate" basis and with the assumption that, although a high proportion of the rods may fail, there is no prolonged heat up of the core in Class 3 through 8 accidents, the containment performance required to meet the "no significant harm" criteria is at least as high as that required to meet the Reg. Guide criteria, i.e., the more conservative assumptions used to evaluate the releases in the Reg. Guide are offset by its less severe criteria for the radiological consequences.

Thus a utility wishing to order a "no significant harm" reactor would have no incentive to seek any relaxation of the present Reg. Guide requirements unless it believed that only a small proportion of the rods would fail in Class 8 accidents.

3.4 Summary of containment performance requirements to be considered in optimization studies

An examination has been made of the dose calculations for Class 8 accidents in a number of FSARs and NRC Safety Evaluation Reports. As might be expected, it has been found that when the doses at the EAB and at the LPZ boundary have been calculated by the methods presented in Reg. Guides 1.3 and 1.4 they are usually relatively close to the criteria laid down in the Reg. Guides (See Appendix 7).

Thus, if it were required that containment performance in Class 3

through 8 accidents should be such that either:

- (a) The requirements of Reg. Guides 1.3 and 1.4 should be met in full, retaining all the present factors of conservatism,

or

- (b) The requirements of the "no significant harm concept" should be met, using "best estimate" calculations,

then there would be little possibility of reducing the performance below that claimed for current designs. However, it was conceivable before the "Three Mile Island" accident, and perhaps might be conceivable again in some years time, that a case could be made for a lower standard of performance. This case could have been made on the premise that the Reg. Guide requirements were unduly conservative for "well-conditioned" accidents in Classes 3 through 8 and that they were leading to substantial expenditure that was not cost effective.

Thus, the optimization of containment systems has been treated in two ways in this study:

Firstly, on the basis that the containment should be cost effective and, if necessary, a lower standard of performance should be accepted in order to achieve cost-effective designs. Secondly, on the basis that the present standard of performance should be maintained.

A possible method of assessing the cost effectiveness of a containment system is as follows:

- (a) The probabilities of the various fault sequences leading to releases of activity are estimated.
- (b) The consequences of the sequences, in terms of harm to health and of property damage are estimated.

- (c) The overall expectation of loss (or "risk") is computed.
- (d) Comparison of the expectation of loss with the cost of the containment system will then show whether the latter can be considered cost effective.

Some preliminary calculations for typical U.S. sites based on Ref. 4 suggests that, if fault sequences more severe than "well-conditioned" Class 3 through 8 accidents could be ignored (on the grounds of very low probability) and expectations of loss are assessed on a "best estimate" basis, then the present standards of containment could NOT be regarded as cost effective. Moreover, if a reasonable allowance were made for the uncertainties in the best estimate values the present standards would still seem to be non cost effective. Thus if containment were optimized on a cost/benefit basis, using expectation of loss (or risk) as a criteria, the correct decision would be to dispense with containment. This conclusion is, in fact consistent with the results of the Rasmussen study (Ref. 3): Analysis of the results presented in Table V 2-1 of WASH-1400 shows that in the case of the iodines released to atmosphere, for example, the "expectation of release" is only decreased by a factor of 10 by the provision of the containment; the risk to the public from nuclear power, as estimated in WASH-1400, is so low compared with the other risks to which they are subjected that a tenfold increase due to the omission of the containment would not be significant.

However, if the cost/benefit analysis were conducted on some other recognized basis (e.g. on the "Minis-Max" principle, which requires the designer to minimize the maximum consequences of accidents) a different conclusion would be reached about the need for containment. As it is highly unlikely that decisions on containment based on a cost benefit analysis founded on the

"expectation of loss" criteria, would be acceptable to the U.S. public, this approach to containment optimization has not been pursued any further in the present study. However, it should be noted that although the notion of an uncontained power reactor is unacceptable in the U.S., this attitude is not universal; in the U.K., for example, it is intended to continue the construction of Advanced Gas Cooled Reactors without conventional containment systems.

References of Section 3.

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4. Possible containment systems for LWR

4.1 Elements of a containment system

4.1.1 Shielding Structure

In order to meet current requirements for resistance to external hazards such as tornados, a reactor in the U.S. must be protected by a substantial shield wall. Even if it were argued that a containment in the conventional sense (i.e. a structure to hold up the escaping primary coolant) were not required, the shielding structure would have to be built as a relatively strong enclosed structure to provide tornado protection. Moreover, since its collapse could endanger vital reactor plant, it would also have to have a seismic resistance comparable to that of the reactor itself. However, in its most primitive form this "containment" could be vented, so that its design pressure need only be as high as required to meet the tornado requirement (about 3 psig), and it could be allowed to have a substantial leak rate.

4.1.2 Vented shielding structure with filtration

In a typical water reactor the primary coolant would contain some active material and, in loss of coolant accidents, some bursting of fuel rods, leading to the release of gaseous and volatile fission products, is to be expected.

Thus one possible step in the evolution of a containment system would be to arrange to vent the primary coolant through some form of filter, thereby removing the immediately available activity and ensuring that the pressure within the shielding structure did not rise beyond its design pressure which, as noted above, could be quite low.

Owing to the very large flow that would have to be accommodated in the event of a large LOCA, it is unlikely that any type of solid filter

(e.g. HEPA or sand-bed) could be devised that would be economically feasible (See Ref. 1). However, in principle, there should be no difficulty in arranging for the coolant to vent through a deep pond of chemically treated water, which could remove a substantial proportion of the volatile and particulate activity.

Nevertheless any rare gas fission products entering the pond would be released and activity released after the initial depressurization could leak out from the shielding structure subsequently. Clearly with one additional refinement of design, namely the provision of a low leak rate shielding structure, some control over the later releases could be achieved. The refinement of design would have to include some provision of resealing the vents. In addition some provision for the removal of heat transferred from the reactor to the shielding structure might be necessary to preserve the integrity of the structure.

4.1.3 Shielding building structure as low-leakage containment

As an alternative to venting the shielding structure it could be used as the basis of a low leakage containment structure. This would necessitate:

- i) Large increase in design pressure, typically this would have to be increased to about 50 psig.
- ii) Provision for removal of heat from the containment to avoid over-pressurization and/or deterioration of materials
- iii) Additional features (e.g. double penetration seals) to achieve a sufficiently low leak rate to make the expenditure on the other additional features worthwhile.

The inclusion of a low leak rate liner in the design might not be

essential. It has been shown in the U.S. that by care in construction a leak rate of less than 1 percent per day (by volume) at a design pressure of about 15 psig can be achieved in a reinforced concrete structure (Ref. 2&3). It has also been shown in France that by the use of a prestressed design, instead of a conventional reinforced one, much lower leak rates should be achievable, down to about 0.5 percent per day, by volume at design pressures up to 5 atm, at least, (Ref. 4). It should be noted that in principle the development of cracks leading to a significant increase in leak rate during service should be much less likely in a pre-stressed structure, since the concrete should be in compression throughout the structure. As discussed in Sec. 5, it is unlikely that there would be a significant difference in cost between a pre-stressed and a reinforced concrete structure built to similar standards of quality and designed to meet the same requirements.

4.1.4 Specially designed very low leak rate, single shell containments

On some sites, in areas of relatively high population density, it is possible that a 1000 MW(e) PWR would require an overall leak rate of less than 0.1 percent per day (at design pressure) from the containment, in order to meet the requirements of Reg. Guide 1.4. This would be beyond the capability of a single unlined structure, unless the pressure within the containment could be reduced very rapidly and/or the concentrations of airborne iodine and other volatile isotopes could also be reduced very rapidly.

As discussed in Sec. 5, the cost of the liner required to reduce the leak rate of the structure to 0.1 or 0.2 percent per day is a substantial component (about 20 percent) of the cost of the system. The cost of a system to reduce pressure and/or iodine concentration rapidly is also significant. (In typical US designs it is about 10 percent of the total

cost of the system, See Sec. 5); devices to limit the leak rate at the various penetrations to an acceptable level would also add to the cost.

Nevertheless, in spite of these sources of additional costs, the single-shell, lined, containment vessel, provided with spray systems to condense steam and to remove iodine, and with internal recirculating cooling and filtration systems has proved to be one of the most popular design solutions for PWR containment in the U.S. However, in order to make use of sites with small distances to the EAB & LPZ, a higher performance may be necessary (See Sec. 6.3).

As indicated in the previous section, it is not expected that there would be much difference in cost between reinforced or pre-stressed, lined concrete shells. It also appears that, subject to certain restrictions (see Sec. 5), a free-standing steel shell, capable of withstanding the same design pressure, and surrounded by a separate shielding structure, of pre-stressed or reinforced concrete, which would simply fill the role defined in Sec. 4.1.1 above, would also cost about the same. A structure of this type is described fully in Ref. 5: it can have the advantage of providing more room for equipment at no greater cost. This similarity in costs makes feasible a further refinement in system design which could lead to significant savings, as discussed in the next section.

4.1.5 Double containment concepts

It was appreciated in the early 1960's that, if very low leak rates were required, an alternative to achieving these by further development of liners and penetration seals would be to use two separate vessels, one enclosing the other, each with a moderately low leak rate and with provision for controlling the activity leaking from the inner vessel into the annular space between the two.

Two methods of controlling the activity in the annular space have been used:

- a) In the first method, the annular space is kept at a slightly negative pressure, so that any leakage through the outer vessel is inwards, by continually pumping the leakage (from both shells) into the inner vessel. In order to avoid over pressurizing the inner vessel in an unacceptably short time, it is necessary for the outer vessel to have a low leak rate, which may add substantially to the cost.
- b) In the second method, the annular space is also kept at a slight negative pressure, but in this case, that is achieved by exhausting air from the inter-space to the atmosphere, via an efficient filtration system. To improve the control, a recirculating filtration system in the interspace may also be provided.

It was shown in the FSAR for the Kewaunee, Wisconsin PWR (Ref. 5) that the combination of a free standing steel inner vessel with a design leak rate of 2.5 percent per day, an outer building of medium quality reinforced concrete and a re-circulating filtration system in the inter-space (with a capacity of 5,400 Cfm) should be sufficient to limit the overall leak rate to less than 0.1 percent per day, at the design pressure (46 psig) of the inner vessel. A similar system was provided for the Prairie Island, Minnesota, PWR. So far as can be ascertained, the design objective was demonstrated during the start-up tests and has been confirmed by tests during service. As discussed in Sec. 5, the use of an inner vessel with a leak rate of 2.5 percent per day as compared with, say, 0.2 percent should lead to a significant reduction in cost. However, the precedent set by the Kewaunee and Prairie Island plants for using double containment to justify a higher design leak rate for the

primary containment does not appear to have been followed in more recent designs. Although the same concept has been used, it has only been claimed as a further, unquantified safety margin when applied in conjunction with primary containment vessels having design leak rates of 0.2 percent per day at design pressure.

4.1.6 Pressure suppression concepts

The rise in pressure within a containment due to the release of steam or of water flashing to steam, can be reduced rapidly by condensing the steam. In the case of boiling water, direct-cycle reactors, the absence of large steam generators external to the reactor pressure vessel (RPV) makes it relatively easy to pass steam released as the result of a primary circuit failure to a static water pool at the bottom of a chamber (the primary containment) containing the RPV and those parts of the feed and steam mains which cannot be isolated rapidly by valves. The escaping steam is bubbled through the water; thereby condensing the greater part of it. The primary containment is housed within a low leak rate structure so that any activity carried over with the uncondensed steam can be held up and treated as in the "dry" double containment system described in the previous section.

This type of pressure suppression has been used for virtually all commercial BWR's and, as described in Sec. 7, probably represents the cheapest way of securing even a moderate containment performance for this type of reactor. If a decision is made to use this type of pressure system at the start of conceptual design, the only parameters which can achieve a significant increase in performance are the leak rate of the secondary containment and the capacity of the plant for treating the air/steam, mixture in the annular space. As discussed in Sec. 7, the cost of the "standard" designs for U.S. commercial BWR's could be

reduced significantly, if a somewhat higher leak rate for the secondary containment were acceptable.

Two other forms of pressure suppression containment have been used for commercial, indirect cycle water reactors. These are:

- i) Rapid dousing by spray, as in some CANDU HWRs.
- ii) Use of an "ice-condenser" as in some PWRs.

In principle, the pool type of suppression system could also be used for indirect cycle reactors and a commercial design for small "integral" PWRs (up to 1200 MWe) has been proposed which uses this type of pressure suppression (Ref. 6).

In the dousing system, as used in the CANDU reactors, the containment structure is a pre-stressed concrete shell, which also embodies a large water storage tank (about 10^5 cu. ft) is in the roof of the building. In the event of a LOCA this is rapidly discharged without delay through spray nozzles, so that the design pressure is substantially lower than in a typical 'dry' PWR containment. Typically, the design pressure is 3 psig as compared with 50 psig (Ref. 7). The pressure is reduced almost to atmospheric so rapidly that a steel liner is not required. This system may be used in conjunction with an external "expansion chamber", of the type described in the next Section, in order to reduce the design pressure still further.

In the "ice condenser" system a large volume of ice, in which flow passages are formed, is maintained in an insulated section of the primary containment structure, which is divided into 3 sections viz:

- a) A lower "collecting chamber" into which all the steam/water mixture from a breach in the primary circuit would pass initially
- b) The "ice condenser section"
- c) An upper receiving chamber in which air displaced from the

collecting chamber, any uncondensed steam, and the air-borne activity released from the primary circuits are collected.

In practice it has been feasible to so arrange the design that virtually all the steam entering the ice compartment would be condensed there. However, some bypassing of steam to the upper chamber cannot be avoided.

By 'doping' the ice with suitable chemicals (e.g. sodium tetraborate) a large proportion of the iodine can be "fixed" immediately in the liquid phase as the steam condenses. This would mean that a higher leak rate might be acceptable, if the iodine release were the dominant contributor to the overall consequences.

In practice the design pressure for a typical "ice containment" for a PWR is usually about 25 psia (as compared with about 70 psia for a typical "dry" containment). This reduction has been achieved in conjunction with a substantial decrease in the volume enclosed by the building.

The 'ice condenser' type of containment can be used in conjunction with either a single shell or double shell arrangement.

Eight land-based commercial PWRs with "ice-containment" have been ordered in the U.S., and at least 4 overseas (2 in Finland, and 2 in Japan); only one of these, the Donald C. Cook plant in Bridgman, Michigan, is operational in the U.S. Numerous difficulties leading to expensive delays have been encountered on the lead plant (Ref. 8) but it is not yet clear whether these are "first of a kind" problems, or are of a more generic nature. Similar problems appear to have been encountered with the 2 reactors which are operational in Finland and Japan.

In considering the optimization aspects of the 'ice containment' system it has to be remembered that the lower containment pressure would lead to some decrease in the rate of re-flooding in the event of a large LOCA. In order to maintain the same safety margin this might

entail a reduction in peak rating or additional plant costs large enough to have a significant effect on the overall cost of the plant.

It should be noted that an advantage of the "ice-containment" and other pressure suppression systems is that since the ratio of the design pressure to atmospheric pressure is well below the critical value, the reduction in leak rate obtained by a given drop in containment pressure is greater than in conventional "dry" containment, particularly in the case of orifice flow. Thus for a given investment in spray system capacity, a greater reduction in leak rate can be achieved.

(4.1.7) External expansion vessel concept

In order to ensure that the leakage of activity from an unlined containment structure is small, it is desirable to reduce the pressure below atmospheric as quickly as possible. As discussed in Section 4.1.6, one way of achieving this is by spray or dousing systems. However, for a site on which several reactors are located, it has been shown by Canadian designers have preferred to supplement the dousing systems by a large evacuated "expansion vessel", into which any of the containment structures on the site can discharge their contents, if the pressure rises above some very low pre-selected level. By an appropriate choice of volumes and working pressure for each of the reactor containment buildings and for the central expansion building, together with a substantial dousing capacity, it is possible to reduce the pressure within the system, after LOCA in one reactor, to sub-atmospheric pressure in less than 1 minute.

As discussed in the next Section 5, this concept appears to offer a significant reduction in cost for a site with three or more reactors, as compared with the conventional, lined, "dry" containment. The system engineered for the Pickering Nuclear Power Station (Toronto, Canada) is therefore described briefly here: The design of containment system for

this plant is based on the principle that "following a hypothetical major failure of any of the high energy systems in the reactor building, accompanied by failure of the reactor protective system, there shall be no significant escape of harmful effluent to the atmosphere. Within less than thirty seconds after any accident resulting in overpressure in a reactor building, the discharge from the failure shall be retained within the containment at sub-atmospheric pressure." (Ref. 3)

A characteristic feature of the system is the use of a common vacuum building which has a clear volume of $2.9 \times 10^6 \text{ ft}^3$ and which is maintained at an absolute pressure of 1 psia (51 mm Hg) to provide the negative containment. This vacuum building is connected through twelve 6 ft. diameter vacuum ducts, and a corresponding set of pressure actuated relief valves, to a pressure relief duct. This duct is in turn connected through 25 ft diameter louvered openings to the "boiler room" of each reactor building. In the top of the vacuum building an emergency water storage tank, containing $350,000 \text{ ft}^3$ of water, is located. This tank provides the water for a dousing spray system which operates if the pressure in the vacuum building exceeds an absolute pressure of 6.2 psi.

A major failure in the high energy systems in any of the reactor buildings will cause the pressure in that reactor building to rise. This rise in pressure will be communicated directly to the twelve pressure relief valves located in the pressure relief duct. The valves respond automatically to a rise in pressure of 1 psi, allowing the effluent to be discharged into the vacuum building. The valve openings are sufficiently large that the pressure in the reactor building affected will be brought rapidly down to the range 0 to -0.5 psig. The outward opening louvers in other reactor buildings prevent them from being

affected by back pressure from the pressure relief duct. The vacuum building and the pressure relief system are designed collectively to contain all the energy which could be released inside a reactor building following any postulated accident to the reactor or its heat transfer system. The containment system has a heat energy absorption capability in excess of two and a half times the amount which is stored in the primary and secondary system.

Because of the very rapid reduction in pressure to less than atmospheric, the uncontrolled leakage from the system should be negligible. However, the design in-leakage could require an extraction rate equivalent to about 5 percent of the total volume per day. With 99 percent efficient filtration system for iodines, this would be equivalent to leak rate of less than .05 percent per day of the air-borne inventory of these isotopes, but 5 percent per day of the rare gases inventory. Presumably this would be reduced in practice by operating with a smaller negative pressure differential.

<4.1.8> Application of the "post-accident" filtration system" (PAFS)
to LWR containment systems

The primary function of the PAFS is to improve the effectiveness of the containment under LOCA condition by providing an external filter through which the air/steam mixture in the containment can be vented to the atmosphere if venting were necessary to prevent the containment from rupturing due to overpressurization. The secondary function of the system is to create a sub-atmospheric pressure within the containment even in situations where the leak rate has increased greatly. In order to meet these basic requirements, the PAFS is designed with separate filtering components and mechanical devices for removing the volatilized fission product particulates and the iodine gases. The selection of

filter components is based on current technology and compatibility with the post-accident environment.

The basic arrangement of the PAFS (Ref. 10), for both PWR's and BWR's, consists of a 4 foot diameter inlet duct system, which connects the containment volume to a sand filter chamber. An orifice typically 1 ft. in diameter for PWR's and 0.6 ft. in diameter for BWR's is placed at the filter inlet to avoid any possible overpressurization of the PAFS. Since the pressure drop across the orifice is substantial, a bypass circuit is used to minimize the fan power requirements when the PAFS is used in its secondary role. The construction of the PAFS is robust enough to withstand the effects of hydrogen burning and large volume flows of moist air.

The design of the post-accident filter for LWR dry containment has only been investigated far enough to see if there would be any major difficulties in its development and to establish its size. Further development work is required to establish a detailed design with adequate performance.

4.2 Performance requirements for LWR containments

4.2.1 Minimum performance required from the containment for a

1,100 MW(e)LWR

It follows from the discussion in Sections 2 and 3 above that the possible range of performance requirements is extremely wide, if the limiting accident to be considered is a "well conditioned" large LOCA.

In order to provide a lower bound to the performance required, it is useful to consider the doses delivered at typical EAB and LPZ distances, using "best estimate" values throughout, for a large LOCA in which all rods are assumed to burst but no containment is provided.

In this case it is necessary to consider the contribution from the short lived gaseous and volatile isotopes. Since, as shown in Table 4.1 below, these make a substantial contribution, particularly to the whole

body dose, if the release occurs within a few hours of shutting down the reactor.

For the source terms the most recent ORNL data has been used for the longer lived isotopes; For the short lived isotopes the fractions percent in the fuel-cladding gap have been estimated in Appendix 3.

It is assumed that the EAB and LPZ are 1 Km and 5 Kms from the reactor respectively. With the assumptions outlined above, the estimated values for the whole body dose and thyroid dose are found to be the values in Table 4.1. No correction has been made for the beneficial effects of radio-active buoyancy.

Table 4.1 Minimum dose at EAB (2 hr exposure) and at LPZ
from an uncontained large LOCA in an 1100 MW(e)
PWR (all rods assumed to fail)

organ effects	Source of radiation dose	Location and dose (rem)	
		EAB ^(a)	LPZ ^(b)
whole body	Rare gases only	100 (rem)	9 (rem)
	Rare gases + volatiles	110	10
Thyroid	Inhaled iodine only	5 700	530

Notes = (a) EAB is at 1 Km from reactor.

(b) LPZ is at 5 Km from reactor.

It will be seen from Table 4.1 that, even this set of assumptions, the requirements of 10 CFR 100 could not be met without a containment

which provided a retention factor of at least 20 for the iodines. However, the values in Table 4.1 are based on the assumption that all the rods burst. Clearly, if it could be shown that less than 5 percent would fail, the requirements of 10 CFR 100 could just be met without a containment. Nevertheless, in view of the uncertainties in the estimated gap releases and in the predictions for the number of rods failing in a LOCA, this could not be regarded as a realistic basis for design.

After a LOCA the volatile isotopes would present a major source of hazard, unless they can be preferentially retained. Consequently the almost immediate removal of iodine and cesium isotopes from the containment atmosphere, which is possible with pressure suppression systems (including the "ice condenser") and the rapid dousing system used in the CANDU reactors, could be a major factor in reducing the degree of leak tightness which is required. In the next Section the minimum requirements for leak tightness are estimated for a containment system in which the volatiles are removed from the containment atmosphere so rapidly that they can be ignored.

4.2.2 Whole body dose due to rare gases as an indication of the maximum acceptable leak rate

The only means of reducing the whole body dose due to the rare gases is to retain them in the containment, while radioactive decay proceeds. Thus it is useful to establish the relationship between integrated leakage of the rare gases and whole body dose at the EAB and at the LPZ. This relationship can then be used to establish the maximum leak rate which could be accepted to meet certain types of criteria (due account being taken of the variation in leak rate with time which is to be expected), if the wind continues to blow in the same direction.

Using the 'best estimate' assumption concerning atmospheric dispersion and the dose/concentration relationship, the integrated leakages of Xe-133 alone which would be required to deliver a dose of 25 rem whole body at the EAB and LPZ are as shown in Table 4.2.

Table 4.2 Integrated leakage of Xe-133 required to deliver 25 rem whole

Body at the EAB (1 Km from reactor) and the LPZ
(5 kms from reactor) (Based on the assumptions of
Reg. Guide 1.4)

<u>Location</u>	<u>Integrated leakage of rare gases to deliver dose of 25 rem whole body</u>
EAB (1 km)	1×10^6 Ci or 0.5 percent of total inventory from fuel
LPZ (5 km)	1×10^7 Ci or 5 percent of total inventory from fuel

We consider initially the situation at the LPZ, as we are concerned primarily with the long term dose. It follows from Table 4.2 that, on the basis of the assumptions prescribed in Reg. Guide 1.3 and 1.4, it would be necessary to limit the release of Xe-133 to less than 5 percent of the total inventory. As the half life of Xe-133 is about 5 days, the original inventory would be reduced by a factor of 20 in 20 days. Thus the maximum acceptable average leak rate over the first 20 days would be in the region of 0.25-0.3 percent per day. In practice the contribution to the whole body dose from the other, gaseous and volatile isotopes, most of which have shorter half lives than Xe-133, might not be negligible, so an average leak rate of less than 0.2 percent per day would be desirable. Depending mainly upon the rate of which the iodines could be removed from the containment atmosphere by the spray system, the requirement not to exceed 25 rem whole body dose at the EAB, in the first 2 hours after the accident, might be more limiting.

With less severe assumptions than those called for in Reg. Guide 1.3 and 1.4, it is clear that release from the containment of a larger fraction of Xe-133 would be possible. For example, the conservatism in the Reg. Guide assumptions concerning factors other than the source term (See Table 2.4) would allow the fractional release of Xe-133 to be increased to 30 percent of the amount released into the containment, without exceeding the 25 rem whole body dose limit at the LPZ. This would allow the average leak rate for the first 20 days to be increased to about one percent per day. In practice, as the permissible average leak rate increases, the relative importance of the other gaseous and volatile isotopes also increases. Detailed calculations show that the average leak rate would have to be limited to about 0.5 percent per day for typical rates of removal of the volatile fission products.

4.3 Performance characteristics of some possible containment systems for PWRs

In the single shell containment systems described in this Section, it should be possible to attain leak rates of less than 0.2 percent per day, although in the case of the pressure suppression types, attainment of such low leak rates would be dependent partly on the successful operation of the pressure suppression system. In the case of "double containments", the parameter of interest is not the observed leak rate from the outer containment, or the exhaust stack discharge, but the "effective leak rate" from the inner vessel to the atmosphere. This can be defined as the ratio (quantity of specific material escaping per unit time) to (total quantity of specific material in system at that time). However, the overall rate of release of filterable activity to the atmosphere would be dependent on the capacity and efficiency of the recirculation filter in the annular space and on the

efficiency of the exhaust filter. Consequently it has not been possible to quote specific values for "effective leak rates". In both "single" and "double" type containments the average leak rate over a period will be dependent on the capacity and efficiency of the containment cooling, filtration and spray systems.

References of Section 4

- (1) "HTR Containment Design Options: An Application of Probabilistic Risk Assessment", GA-A14147, UC-77, August 1977.
- (2) Final Safety Analysis Report of Kewaunee Nuclear Power Plant, Vol. 2, January, 1971.
- (3) N. S. Haines et al., "Design Features and Concrete Requirements for a Vacuum Building at the Pickering Nuclear Generating Station", PAPER SP 34-62, Conf. on Concrete for Nuclear Reactors.
- (4) J. L. Costaz and J. Picant, "Multiple-barrier Containment without steel liner", Conference on Experience in the Design, Construction and Operation of Prestressed Concrete Pressure Vessels and Containments for Nuclear Reactors, September, 1975.
- (5) Final Safety Analysis Report of Kewaunee Nuclear Power Plant, Vol. 4, January, 1971.
- (6) W. C. Siler and R. S. Marda, "Containment for Small Pressurized Water Reactors", ANS Trans. 27, November, 1977.
- (7) "CANDU 600 MW(e) Standard Design", Nuclear Engineering International, 19, June, 1974.
- (8) Final Safety Analysis Report of Donald C. Cook, Nuclear Power Plant, January, 1972.
- (9) C. L. Moon, "Pickering Generating Station", Nuclear Engineering International, 15, p. 501, June, 1970.
- (10) B. Gossett, L. Cave, C. K. Chan, D. Okrent, and I. Catton, "Post-Accident Filtration as a Means of Improving Containment Effectiveness", Proceedings of Topical Meeting on Thermal Reactor Safety, August, 1977.

5. Optimization of Containment Design for Class 3 through 8 Accidents (PWR)

5.1 Factors Affecting the Optimization

Three different aspects have to be considered

- (a) Performance requirements
- (b) Relative costs of different design features
- (c) Constraints imposed by other requirements (e.g. seismic resistance required from the structure)

Each of these aspects is discussed in the following sections so far as they relate to the optimization process for a PWR. The corresponding problems for a BWR are discussed in Section 7.

5.2 Performance Requirements

As discussed in Sections 2 and 3, it is difficult to decide what minimum performance would be required from the containment of a PWR to be licensed in the U.S. in several years time: if NRC retain requirements for the source terms similar to those currently set out in Reg. Guide 1.4 (Ref. 1), there would be little opportunity for relaxation of the present performance requirements; similarly, a utility seeking a "No significant harm" plant would not wish to relax the present requirements, unless it believed that very few fuel rods would fail in a LOCA. Accordingly, the possibilities for re-optimization have been examined in three ways.

- (i) By considering how the existing requirements imposed by Reg. Guide 1.4 might be met by the use of existing concepts applied in different ways, and the extent to which it might be possible, by further work and analysis, to demonstrate that lower leak rates could be achieved than

have been claimed in the past.

(ii) By examining the cost of arrangements which would provide limited or moderate degrees of retention.

(iii) By examining the cost of arrangements which could provide higher degrees of retention, or the same degree with higher reliability, than is necessary to meet the requirements of Reg. Guide 1.4 in order to meet a possible demand for a "no significant harm" reactor design.

5.3 Cost of the basic elements of containment systems

The only comprehensive sources of information of reasonably up to date LWR costs which are readily accessible is provided by Refs. 2 and 3. These are based on 1976 data and have the disadvantage that for some parts of the plants, the information is not broken down into sufficiently fine detail. The more extensive data on containment cost compiled by ORNL in 1970 is based on 1963 cost data and much smaller reactors, so that these data are now of limited value. A more up to date compilation has recently been completed by DOE but the final report is not yet available (Ref. 4). Some additional information on specific points has been obtained from utilities and architect engineers but it is difficult to be sure that the prices quoted relate to the same time frame as that on which Ref. 2 and 3 are based.

The basic data on containment prices which have been extracted from Ref. 2 and 3 are shown in Table 5.1. It will be seen that there is broad similarity between the individual items of data for PWR's and BWR's.

By taking account of the differences in dimensions and design pressures of the two containment systems to which these data relate, it has been possible to confirm that the cost relationships are

sufficiently linear to estimate the cost of possible arrangements that appear to be of promise from the point of view of cost or performance.

Table 5.1 Summary of Containment Structure Costs (1976 Dollars) from ERDA Report(Ref. 2*) for PWR, steel lined, single R/F concrete shell

<u>Component of Structure</u>	<u>Cost \$ m (million)</u>
Substructure concrete & R/F**	2.3 m
Interior concrete and R/F**	8.5 m
Superstructure concrete and R/F (Shell & Dome)	10.9 m
Liner (Including stiffener and penetrations)	10.5 m
Other items(e.g. structural steel & painting)	1.0 m
Total	33.2 m

The total cost of the structure is about 6 percent of the capital cost of the plant.

Notes

*Estimated prices exclude interest during construction and escalation since 1976. Engineering costs are not included in the above figures but can be accounted for by increasing them by 30 percent.

** Costs include labor, formwork, cadvelds and minor items such as embedded steel fittings.

Other data from the ERDA report.

From the ERDA report it can be deduced that:

(i) A "standard" figure of \$35 per c. yard for concrete (as placed) has been used.

(ii) For steel reinforcement (as placed) the cost varies between

\$800 and \$1200 per ton.

(iii) "Reinforcement ratio" (tons of steel/c yd concrete, is about 0.2 for the substructure and interior and about 0.33 for the shell. For "tornado and seismic proof" buildings, it appears to vary from about 0.1 to 0.2.

To obtain some appreciation of the absolute level of containment costs, it should be noted that the corresponding total direct capital cost for the plant is estimated to be \$421 or \$370 per kw.

Some additional cost data have also been obtained from Southern California Edison (Ref. 5). These data are based on the actual costs of the containments for San Onofre reactors 2 and 3 and reflect the high seismic accelerations for which the plant is designed. However, it is possible to deduce from these data that the cost of the penetrations for the "Reference Design" of Ref. 1 would be about \$1.5 m, when expressed in a comparable manner.

Some data relating to the cost of the support systems for the containment can also be derived from Refs. 1 and 2. For PWR these are shown in Table S.2.

Table S.2 Summary of Containment Support System Costs (1976 dollars)
for PWR, from ERDA Report (Ref. 2)

System	Cost \$m(million)
Pre-entry purge system	0.17
Refuelling purge system	0.35
Containment recirculating filter system	0.24
Recirculating cooling system	1.47
Containment spray system (2x3000 GPM)	4.18
TOTAL -	5.41

Note: The above costs do not include essential additional items such as cabling, switchgear and emergency power sources. These would add about 10 percent to the total cost. Thus the total cost of the containment, including supporting systems and services, is about (as defined in Ref. 2) \$40 million, or 10 percent of the direct capital cost.

(5.4) Constraints other than leak tightness imposed on the optimization of containment systems

(5.4.1) Constraints on structural design

As indicated in Sec. 4.1.1, it would be difficult to avoid a situation in which some form of shielding structure around the reactor and steam generating units could be dispensed with, as protection must be provided against radiation, missiles, tornadoes, and possibly vapor cloud explosions and aircraft crash. Owing to its proximity to the reactor, this structure would also need to have seismic resistance comparable to that of the reactor itself. The effects of these constraints on cost are discussed in more detail in Section 5.5.

(5.4.2) Effects in service inspection, maintenance and repair

Although the discussion in the next Section suggests that a reduction in containment size could lead to a reduction in cost, it is necessary to ensure that there is adequate space to carry out in-service inspection, maintenance and repair. In particular the increasing concern about the doses received by maintenance workers, emphasizes the need to leave sufficient space to erect temporary shielding, during some maintenance operations.

(5.4.3) Effects on construction program

In addition to the operational reasons which militate against size reduction there is also likely to be a strong inter-relation between space available within the containment and plant construction time. Owing to the high cost of delays, in terms of interest charges and differential generation costs as compared with fossil plant, savings on capital cost that might appear significant could be offset by these factors.

5.5 Possible means of reducing containment costs

By further summarizing of the cost data presented in the previous Section, the main elements of the cost of a "dry" containment for a PWR can be demonstrated more clearly, making the possibilities for any significant reduction in cost readily apparent.

From Table 5.1 it will be seen that the main costs, apart from the engineered safety systems of a "dry" containment from a PWR are, in effect:

(i) Containment "envelope": structure	\$10 million
(ii) Containment liner (including stiffeners and penetrations)	\$10 million
(iii) Containment structures not dependent on leak rate or design pressure	\$10 million
(iv) Containment support systems	\$ 6 million

Thus a major item on which savings might be made in the cost of a conventional containment is the liner. Two solutions appear to be possible:

- (a) Dispense with the steel liner, possibly using some elastomeric coating in its place.
- (b) Replacement of the single shell structure by a double shell, both unlined, with pump-back from the inter-space into the primary vessel, or with a filtered extract facility.

It is doubtful whether a single, unlined, shell would be a practicable design solution, as the leak rate would be difficult to predict, owing to the effects of quite small errors in construction. Rectification (e.g. by pressure grouting) would be feasible but could lead to expensive delays in construction. However, if it were feasible

to design on the basis that release of virtually all the rare gases could be permitted, providing that the high proportion of the volatiles was retained, or that the pressure could be reduced to subatmospheric within a few minutes, then an unlined single shell might be an acceptable solution.

The extent of the savings in cost which might be achieved by dispensing with the liner are estimated in Sec. 6, below.

About 60 percent of the cost of the containment "envelope" structure (i.e. the cylindrical shell and dome), for a given diameter is proportional to the design pressure, as this determines the amount of reinforcing steel required (See Appendix 8); the remaining 40 percent is largely attributable to the mass concrete and the formwork, which will not vary greatly in cost with design pressure.

Thus in a design, such as an "Ice Containment" in which the design pressure could be reduced by, say, a factor of 4, there could be a significant reduction in the civil costs. However, these could be offset to some extent, by additional plant costs. A quantitative estimate of the savings is given in Section 6 below. The methods for reducing design pressure which have been used in the CANDU PHWRs are also of interest in this respect.

In case of an unlined, double-shell design, the design-pressure for the inner shell would have to be the same as for a single-shell design but the strength of the outer shell would be determined by other site related factors, such as: tornado and seismic resistance.

Resistance to missile impact and aircraft crash

Resistance to effects of vapour cloud explosions

At first it might appear that of these factors, only seismic effects would be likely to lead to damage to the reactor and to the containment simultaneously. However, if the problem is viewed probabilistically it will be seen that all these factors could lead to situations in which electrical and other vital supplies could be effected, so that there would be a greater chance of Class 5.2 accidents (large "pressure" transients that induce fuel failure above those expected and steam "generator" leaks") and of Class 8.1 accidents (small LOCA due to safety or relief valves lifting) occurring under adverse system conditions. Thus the outer shell would probably have to be a strong structure and could contribute significantly to the cost of the containment system. Estimated cost savings are given in Section 6.4 below.

In the case of a containment system which requires an expensive liner there is an economic incentive to keep the radius of the containment structure small: it is shown in Appendix 8 that for a given release of energy into the containment, the cost of the steel required for the pressure resistance purpose is virtually independent of radius, but the cost of the liner is approximately proportional to (radius)².

From the break-down of costs in Table 5.1 it can be shown that a ten percent increase in radius of a steel lined containment vessel would add about 6 percent to the cost of the structure. Nevertheless, consideration of access for inspection and repair necessitate some minimum internal radius.

It is also shown in Appendix 8 that, if a steel vessel is employed for the pressure retaining role, there is likely to be an economic advantage in using a spherical vessel in preference to a cylindrical one and, in this case, the potentially cheaper design would provide

more useful space for plant and equipment within the contained volume. An important factor here is that the thickness of the shell should be less than 1.5 in., so that stress-relief after fabrication in the field is not mandatory.

It also follows from the preceding argument that the use of unlined single or double-shell containments should also permit an increase in the amount of useful space within the containment, without increasing the cost of the pressure retaining structure. However, the increase in the dimensions of the outer shell of a double-shell containment could add significantly to its cost, as the proportion of reinforcement to concrete would remain approximately constant.

References of Section 5

- (1) U.S. NRC Regulatory Guide 1.4, "Assumptions Used for Evaluating the Potential Radiological Consequences of a Loss of Coolant Accident for Pressurized Water Reactors," June, 1974 (Revised 1977).
- (2) "Capital Cost: Pressurized Water Reactor Plant," NUREG-0241, NRC-2477-5, June, 1977.
- (3) "Capital Cost: Boiling Water React Plant," NUREG-0242, NRC-2477-6, June, 1977.
- (4) Private Communication, R. L. Allen (United Engineers and Constructors Co.)-L. Cave (Sept. 1979).
- (5) Private Communication, E. Cramer (Southern California Edison Co.)-L. Cave (Aug. 1979).

5. Estimated costs of some possible containment arrangements for PWR relative to the standard 'dry' containment system of Ref. 1

6.1 Introduction

In this section 4 different classes of arrangement are considered. These are as follows:

- Class 1. Arrangements other than the "Reference design" (i.e. the lined reinforced concrete shell of Ref. 1) already being used in U.S. reactors which should lead to reduced costs.
- Class 2. Changes to existing arrangements, which could lead to a higher standard of performance that is required by Reg. Guide 1.4 (Ref. 2).
- Class 3. Arrangements currently in use in foreign reactors, but not in use in the U.S., which could lead to a significant reduction in cost while complying with the requirements of Reg. Guide 1.4.
- Class 4. Arrangements, not currently in use in any commercial reactors, which would lead to a substantial reduction in cost but would not necessarily meet the requirements of Reg. Guide 1.4.

Examples of each of these classes are given below; the estimated cost changes are shown as a percentage of the total containment cost (as estimated in Ref. 1), which is itself estimated to be about ten percent of the direct capital cost of the nuclear station.

(6.2) Relative cost of licensed alternatives to the reference design

(6.2.1) Containment systems used in PWR's now in operation, or under construction in the U.S.

The following arrangements are in use in plants licensed since 1968.

(a) "Dry" containments

- (i) Single mild steel (MS)-lined, reinforced (RF) concrete shell
(i.e. the "Reference Design").
- (ii) Single MS-lined, prestressed (PS) concrete shell.
- (iii) Free-standing, steel cylindrical vessel, with surrounding sealed or unsealed shielding structure, or "silo".
- (iv) Free standing steel cylindrical vessel in a closed, unlined, moderate leak-rate silo.
- (v) Free standing, steel, spherical vessel with closed, unlined, moderate leak-rate silo.
- (b) Ice condenser containment
 - (i) Free-standing steel cylindrical vessel, with closed, unlined, moderate leak-rate silo.
 - (ii) Single, MS-lined, RF concrete shell.

(6.2.2) Relative costs of the various "dry" containment arrangements

TVA have costs of single shell, lined designs using both pre-stressed and reinforced concrete and have concluded that there is no significant difference in cost (Ref. 3).

TVA have also found that a plant with a low leak rate spherical steel vessel design, enclosed in an unlined concrete silo, is no more expensive than one with the Reference Design type of containment (Ref. 3) which suggests that the two types of containment are similar in cost. This conclusion was also reached by Duke Power Company, from their analysis of the extensive experience gained in West Germany with this type of design (Ref. 4). Thus it appears that the free-standing spherical steel vessel has the advantage that the increased control over leakage which is obtained by the use of the dual containment concept is secured without additional cost, whereas to provide the additional silo structure

in the Reference Design would increase its cost significantly viz:

Allowing a 4 feet annular space between inner and outer vessel would lead to an inner diameter for the silo of 148 feet, compared with 140 feet for the Reference design.

Scaling the cost of formwork and concrete as $(\text{radius})^2$, but reducing the proportion of reinforcement to concrete from 0.33 to 0.2 (as in the silo for the Reference Design BWR) leads to a cost of about \$9 million for the silo, with the assumptions of Ref. 1.

This increment of \$9 million is about 25 percent of the cost of the Reference Design "building structure" (A/C Item No. 212.1 of Ref. 1). Consequently, if there were a demand for higher performance, the use of a spherical steel vessel, with enclosing silo, should provide a containment system about 20 percent less expensive than the Reference Design plus an enclosing silo.

Although free standing cylindrical steel designs were used for the second generation of PWR's, these are not attractive for the large plants of the present generation, as the thickness of steel for the cylindrical portion is too great to avoid the need for stress relief in the field, after fabrication, unless the radius is made excessively large.

(6.2.3) Relative costs of "ice condenser" containment

It is understood that the initial incentive for the development of the "ice condenser" was to reduce cost. From the point of view of the containment structure itself, the saving should be considerable, viz:

The radius of the cylindrical portion of the pressure retaining structure can be reduced to 57.5 ft. for a 1,100 MW(e) reactor (Ref. 5) at a design pressure of 28 psia. At this design pressure, cylindrical steel vessels, with separate silos, and single shell MS lined, concrete vessels are equally feasible.

Using the data of Ref. 1, it is found that the reduction in the cost of the containment super-structure (including MS liner or free standing steel vessel) as compared with the Reference Design, should be about \$6 million, or 18 percent of the total cost of the "building structure" (A/C Item No. 212.1) of the Reference Design. It has not yet been possible to obtain data on the additional cost of the "ice condenser" itself but as the racks and baskets for the ice are "factory items" they should not add substantially to the cost. Similarly, the chiller units required to maintain the ice are a standard industrial product. Overall it is likely that the additional costs would be between \$1 million and \$2 million. The conservative assumption has been made that the cost of the containment support systems would not be reduced; in practice the capacity and cost of the recirculating cooling system and the containment spray system could presumably be reduced.

Thus the net saving is probably about 15 percent of the total cost of the Reference Design. However, if the use of a plastic liner, in conjunction with a pre-stressed concrete shell, could gain acceptance in the US, the net saving could be increased to about 25 percent. In order to make use of a plastic liner, means for preventing the long term sharp rise in pressure (See Ref. 5) would probably be necessary. The rise occurs when the last of the ice melts. It should be possible to achieve this by an increase in the containment spray system capacity. That is, the use of the ice condenser pressure suppression system could lead to savings similar to those obtainable with the Canadian high dousing rate pressure suppression system. (See Sec. 6.4.3, below)

(6.3) Minor modifications to licensed designs which could lead to a
higher standard of performance and/or lower cost

(6.3.1) Double shell systems with improved control over annular leakage

In principle the double shell systems currently offered could give an improved performance over the single shell designs, which are being offered at the present time, since at relatively little additional cost the capacity of the recirculation and exhaust systems for the annular space between the shells could be increased substantially e.g., increasing the efficiency of the exhaust system filter by a factor of 3 should reduce the net leakage by a comparable factor, but it should not increase the total cost of the containment system by more than \$0.5 million (i.e., about 1 percent).

(6.3.2) Double shell system with lined inner and outer shells

By the use of an outer shell with a relatively low leak rate (see below) the in-leakage could be reduced to a level at which a "pump-back" system, instead of an exhaust system, could be used to control the activity buildup in the inter-space for the first 48 hours, at least.

In principle the use of a pre-stressed outer shell with an elastomeric liner should be sufficient to reduce the in-leakage to less than 0.5 percent of the annular volume per day (See Section 4.1.5), which should make pump-back feasible for at least 48 hours. Alternatively, in view of the doubts apparently entertained in the U.S. about the use of plastic liners, a mild-steel liner could be considered for the silo, as well as for the pressure retaining shell.

The additional cost of the elastomeric liner, extrapolated from Canadian experience, should be about \$0.5 million (i.e., about 1.5 percent of the "building structure" cost for the Reference Design) whereas

the mild steel liner would add about 25 percent to the cost of the "building structure". In view of this large difference in cost, further consideration of the effectiveness of a plastic liner in the less severe environment provided by the annular space would be worthwhile, if there were an interest in "pump-back" systems.

(6.3.3) Partial dual containment systems

A method for obtaining most of the advantage of a dual containment system at relatively little cost has been suggested by Stone and Webster Engineering Corporation (Ref. 6). The proposal is based on the concept that the greater part of leakage from a single shell containment structure is likely to occur in the vicinity of the penetrations. As these can be located relatively near the ground level, and concentrated azimuthally, it is possible to provide dual containment in these areas at lower cost.

Alternatively, on sites where higher performance is particularly important (e.g. short distances to EAB or LPZ) the same basic design can be adapted readily to full dual containment.

By utilizing the reactor buildings surrounding the lower part of the primary containment shell, which have to meet the same strength requirements as a containment silo, as the basis of the partial dual containment, the additional cost of reducing the leak rate to a sufficiently low level and providing the necessary filtered recirculation and exhaust systems would be unlikely to exceed \$1 million. Thus most of the benefits of a double shell system should be available for about 10 percent of the cost of the sealed silo required for a dual system, i.e., the higher performance could probably be attained at a cost of less than 3 percent of the containment "building structure" of Ref. 1 instead of 25 percent. However, this

approach introduces a further element of commercial risk since, if a leak should occur in the "single shell" part, this might be difficult to locate, leading to a delay in reaching commercial operation.

(6.3.4) Relaxation of leak rate requirements for mild steel liners

A further alternative approach, which could lead to a significant reduction in the cost of double shell systems which embody a mild steel lined inner shell, is to accept a higher leak from the inner shell and to place greater reliance on the filtered recirculation and exhaust system provided to control activity in the annular space, as in the case of the Kewaunee and Prairie Island plants (See Section 4.1.5). For example, in the case of Kewaunee it was shown that the requirements of 10 CFR 100 could be met if the leak rate of the primary shell were as high as 2.5 percent per day at design pressure. At this level of leak tightness it should be possible to dispense with the "leak chasing" channels provided for each weld of a mild steel liner. The saving which might be made by this change is estimated to be about 15 percent of the cost of the liner, or about 4 percent of the cost of the "building structure" defined A/C No. 212.1 of Ref. 1.

(6.4) Some foreign containment systems with potential for cost reduction

(6.4.1) Double containment using unlined inner and outer shells

As shown below the modification introduced by the French utility (Electricite de France, EDF) for their 1,300 MW(e) PWR, embodying an inner, unlined, prestressed concrete shell and a reinforced concrete outer shell, also unlined, seems unlikely to lead to a saving in cost, if used in the U.S., as compared with the "Reference Design".

For an 1,100 MW(e) PWR with dry containment the major changes in the cost would be:

- a) Elimination of liner of inner shell, saving \$7.5 million
- b) Introduction of outer shell, or silo, adding \$9 million

(See Section 6.2.2)

There would also be some increase in cost due to:

- (i) Increased cost of penetrations, as "guard tubes" would be required in the annular space.
- (ii) Increased cost of foundations, due to greater area and substantial increase in loading.
- (iii) Increase in plant costs due to increased length of pipe work and cabling.

*Note: Allowance made for increase in cost of formwork, in absence of steel liner.

Thus in US conditions it is likely that there would be an increase in cost of at least 5 percent, compared with the single shell, concrete clad Reference Design.

In practice EDF too have found that there is virtually no saving in their conditions, and that decreased flexibility of the construction program, as compared with that for the single, lined shell system makes this approach unattractive (Ref. 7). However, a possible modification of this approach, which might be utilized for U.S. sites where a single shell system might not meet 10 CFR 100, can be visualized. This is discussed in Section 6.5 below.

(6.4.2) Use of low pressure systems incorporating a vacuum relief vessel

If the Canadian system described in the Section 4 above were applied in U.S. conditions to an 1,100 MW(e) PWR, the major changes in cost would be:

- (a) Removal of mild steel liner (less penetrations)
from reactor containment shell , saving \$7.5million (net)
- (b) Reduction in reinforcement for reactor
containment shell from 0.33 tons per cubic
yard to 0.2 tons per cubic yard of concrete, saving \$2.8 million
- (c) Addition of vacuum relief vessel, cost { additional cost, see
shared between several reactors , { below.
- (d) Addition of interconnecting ducts, { additional cost, see
duct isolating valves and control system, { below.

The cost of the additional items has been estimated as follows:

The vacuum building for a CANDU 500 MW(e) PHWR has a free internal volume of $2.9 \times 10^6 \text{ ft}^3$. The volume of each reactor containment shell is $1.8 \times 10^6 \text{ ft}^3$. These volumes are to cope with a large LOCA accompanied by loss of all the secondary coolant from one reactor. It is assumed that the reactor containment shells would be the same size as in the Reference design and that in the U.S. it would not be necessary to cope with the water from more than one secondary loop, in addition to the primary coolant. As a first approximation a vacuum building with same free volume as the Pickering plant should be sufficient (i.e. $2.9 \times 10^6 \text{ ft}^3$).

The cost of the vacuum building has been estimated as if it were an unlined silo building, except that allowance has been made for the stiffening effect of the internal reinforced concrete grid structure used in the Canadian design. It has been assumed that the total amount of reinforcement would be the same as in a U.S. containment silo of the same volume. These costs are estimated to be:

	m = million
vacuum building, substructure	\$ 2.5 m
" " external structure	\$ 7.1 m

"	", internal structure	\$4.1 m
Reinforcement for internal and external	structure	\$6.6 m

Total additional cost \$20.4 m

The inter-connecting ducts would require about 100 tons of steel per reactor: the cost of these together with the valves and the other items would be unlikely to exceed \$1 m, i.e., it would be barely significant in comparison with the cost of the vacuum building.

Thus the additional cost to be set against the saving of \$10 million per reactor would be about \$21 million overall. Thus there would be a saving only for stations with 3 or more reactors.

Overall, therefore, this Canadian system should provide a potential saving of about 11 percent of the total cost of the containment systems of the Reference Design, for a 4-reactor station.

(6.4.5) Use of rapid dousing systems for pressure suppression

The Canadian concept, for single reactor stations, of extremely rapid condensation of steam released in a LOCA, permits the use of a plastic lined, low pressure containment building.

If the concept were applied to a U.S. 1,100 MW(e) PWR, the cost savings would be as follows:

m = million

Removal of mild steel liner (less penetrations) Saving of \$7.5 m (net)

Reduction in reinforcement from 0.33 tons/cu.yd

of concrete to 0.2 tons/cu. yd.

Saving of \$2.8 m

The additional cost of the plastic liner is estimated to be \$0.5 million (See Sec. 6.3.2). The dousing system, per se, would probably cost no more than the conventional spray system of a typical U.S. PWR, but

carrying the weight of water at the top of the reactor building might slightly increase the cost of the structure. Overall it appears likely that a saving of about \$10 million per reactor (i.e., about 25 percent of the total cost of the containment system of the "Reference Design") might be achieved.

(6.5) Estimated costs of some simple containment systems

(6.5.1) Shielding structure with no retention or dousing capability

The structure visualized would be used simply to protect the reactor from external hazards such as tornadoes, aircraft and missiles from turbine break-up. To be fully effective as a shield, the building would have to be closed but to avoid having to design for high transient pressures in the event of a large LOCA it would also have to be vented. The cost can therefore be estimated in the same way as that of the "silo" described in Sec. 6.2.2, above, except that it need be no larger than the shell of the "Reference Design". Thus the cost of the shielding structure alone would be about \$8 million, which would represent a saving of about \$14 million, or 35 percent of the total cost of the Reference Design containment system; there would be a further saving of about \$4 million on the support services which, in this hypothetical case, could also be regarded as superfluous. Thus the upper limit to the possible saving is about \$18 million, or approximately 50 percent of the total cost of the Reference Design containment system.

(6.5.2) Vented containment (unfiltered)

The only substantial saving achieved by the use of the vented containment concept would be the reduction in cost of reinforcement and, if a lower performance were acceptable, the cost of a low leak rate mild

steel liner (less the penetrations) as this could conceivably be replaced by a mild steel liner with a high leak rate, or by a plastic liner. It follows from earlier examples that the savings would be as follows:

m = million

with low leak rate steel liner \$2.8 m or 7% of Reference Design Cost

with high leak rate steel liner \$4.6 m or 11% of Reference Design Cost

with plastic liner \$10 m or 25% of Reference Design Cost

The cost of some form of re-sealing vent would have to be set against these savings; however this would be unlikely to increase the cost by more than one percent of the total system cost.

(6.5.3) Pressure suppression systems

As discussed in Section 6.4.3, the maximum use of the pressure suppression concept, as in Canadian reactors, could lead to a reduction of up to 25 percent in the total cost of the containment system. In principle, there is no reason why a comparable saving should not be obtained with other pressure suppression systems, such as the "ice condenser" or a water pool in the base of the containment building. In both of these cases, the proportion of iodine in the containment atmosphere would be reduced to the order of 1 percent almost immediately and the low maximum overpressure could be brought down nearly to atmospheric within a few minutes by a supplementary dousing system. In these circumstances, the combination of a pre-stressed concrete shell and a plastic liner should limit the leakage rate to the order of 0.1 percent per day within a few minutes.

Taking the "ice condenser" of Section 6.2.3 as an example, the saving, as compared with the total cost of the Reference Design containment system could probably be increased from 15 percent to about 25 percent.

(6.5.4) Further exploitation of double shell concept, at low cost

As noted in Sec. 6.3.1, the French experience has been that the use of the unlined double shell containments concept does not lead to any worthwhile saving. However, if pre-stressed concrete structures were used for both the inner and outer shells and both were provided with plastic liners, it is likely that the leakages into the annular space would be sufficiently low to permit the system to be operated in a "pump back" mode for 2 or 3 days, at least, so that the discharge of rare gases to the atmosphere could be reduced. Thus at a cost about 7 percent higher than that of the Reference Design, it should be possible to obtain a marked improvement in performance, which would be useful if a utility wished to build a "no significant harm" reactor. If the pressure suppression concept were also employed the overall cost should be virtually the same as that of the Reference Design. The use of the pressure suppression concept would also facilitate the use of plastic lining, as the temperatures within the containment would be substantially lower.

6.6 Spray cooling capacity as a factor in containment optimization

It is claimed for the CANDU single-reactor plants that the rapid dousing system provided is sufficient to arrest the rise in pressure within the containment following a large LOCA and to limit the peak pressure to a few psi. It has not yet been possible to find a detailed analysis of the system in the published literature but it appears that an initial flow rate of order 10^5 gal. per min. is employed for 500 MW(e) PHWRs. Using this flow rate it is possible to determine whether the adoption of this concept in the Reference Design would be advantageous. In the Canadian designs, a high initial rate of cooling is achieved by the discharge of water from storage tanks located high up in the building,

whereas in the US designs a pumped spray system has been preferred. This it is necessary to establish the cost of increasing substantially the capacity of the containment cooling system in the US system.

From Ref. 1 it has been found that 90 percent of the cost of the US containment spray system is attributable to pipework and valves and 10 percent to the pumping equipment (i.e. about \$0.5 million). Given sufficient head from the pumps, it should be possible to increase substantially the rate of delivery from the existing pipework, thus a 10-fold increase in flow rate would probably cost about \$5 million.

The flow rate of the system costed in Ref. 1 is 6000 gal. per min., thus in order to use the rapid dousing concept this would have to be increased by an order of magnitude; with a pumped system this would lead to a cost increase of about \$5 million. If the only gain were a reduction in design pressure of the containment structure, the change would not be worth while, as the maximum saving would be less than \$3 million, which could be achieved by a reduction in the quantity of reinforcement. However, if the Canadian practice of using a plastic liner were adopted, there could be an overall saving. However, this would be unlikely to exceed 10 percent of the total cost of the Reference Design.

6.7 Effect of the "Post-accident Filtration" concept on containment costs

In the usual scenarios for accident Class 3 through 8 it is not visualized that containment pressures would rise beyond the design pressure level, since it assumed that the various engineered safety systems will function as designed.

Consequently, with a remit to design for accidents no more severe than a well-conditioned large LOCA, the designer would have no need to consider the provision of a post-accident Filtration system (PAFS) such as that discussed in Ref. 8, in the containment.

It should be noted that a PAFS such as that described in Ref. 8 would not have sufficient capacity to reduce the initial *maximum* pressure within the containment to any significant extent and thus could not be used to reduce the design pressure and thereby reduce the cost of the containment structure.

(6.8) Summary

The cost savings of some possible containment arrangements, expressed as a percentage of the total cost of the "Reference Design" containment system for PWRs (Ref. 1), are summarized in Table 6.1 for single and double containments.

References of Section 6

- (1) "Capital Cost: Pressurized Water Reactor Plant," NUREG-0241, COO-2477-S, June, 1977.
- (2) U.S. NRC Regulatory Guide 1.4, "Assumptions used for Evaluating the Potential Radiological Consequences of a LOCA for PWR's," June, 1974 (Revised 2).
- (3) Private communication, Mr. R. G. Damer (TVA)-L. Cave, September 1979.
- (4) Godfrey, A. D., Schabert, H. P., and Loef, W. A., "The Spherical Containment System for 1300 MW(e) PWR's," Proc. of the Amer. Power Conference, Vol. 37, 1975.
- (5) Final Safety Analysis Report of Watts Bar Nuclear Plant, 1976.
- (6) Kent, G. F. and Wille, D. J., "Design and Licensing of a Flexible Containment Approach," ANS transaction, Vol. 27, 1977.
- (7) Private communication, Dr. Backer (EPRI)-L. Cave, August 1979.
- (8) B. Gosset, L. Cave, C. K. Chan, D. Okrent, and I. Catton, "Post-Accident Filtration as a Means of Improving Containment Effectiveness," Proceedings of Topical Meeting on Thermal Reactor Safety, Sun Valley, Idaho, August, 1977.

Table 6.1 Possible Cost savings for PWR Containments,
expressed as a percentage of total cost of
Reference Design Containment (Ref. 2)

Serial No.	Style of Containment	Description of Containment	Cost Saving per cent
1	Single	Hypothetical unconfined shielding structure	50
2	Single	Vented containment, unfiltered	
		i low leak rate MS liner	7
		ii moderate leak rate MS liner	11
		iii plastic liner	25
3	Single	Reference design, but relaxation on leak tightness of MS liner	5
4	Single	Pressure suppression system using the ice condenser concept	
		i low leak rate MS liner	15
		ii plastic liner	26
5	Single	Pressure suppression system, using Canadian high dousing rate concept and plastic liner	25
6	Single	Pressure suppression system, using Canadian vacuum relief vessel concept for multi-reactor station	11
7	Double	Spherical steel vessel with unlined R/F concrete silo; (Increased space for contained plant; potential for reduction in effective leak rate)	net

Table 6.1 (continued)

Serial No.	Style of Containment	Description of Containment	Cost Saving per cent
8	Double	As Serial No. 7, with plastic lined P/S concrete vessel	1 to 2
9	Double	Unlined inner and outer concrete shells (P/S and R/F)	No saving in U.S. conditions (Increase of 5 per cent)
10	Double	Plastic lined inner and outer P/S concrete shells (Potential for use of pump-back concept)	(Increase of 7 per cent)
11	Double	Reference Design (Inner vessel, MS lined R/F concrete) with unlined silo vessel (Potential for reduction in effective leak rate)	Increase of 20 per cent
12	Partial	Reactor building adopted to role of half-height outer building (Potential for reduction in effective leak rate)	Increase of 3 per cent
13	Single	Pressure suppression, using Canadian high dousing rate concept with U.S. type pumped water supply. (i) with MS liner (ii) with plastic liner	Increase of about 5 per cent Saving of about 10 per cent

(7). Estimated costs of BWR containment

The ERDA/NRC analysis of BWR capital costs (Ref. 1) shows that the total cost of the Mark III BWR/6 containment system is about 20 percent higher than that for a PWR "dry" containment of the same power output. These are no comparable cost data for the earlier types of BWR containment (Mark I and Mark II), but from the detailed discussion in Ref. 2 seems most unlikely that the vendor would recommend the further use of the earlier designs.

Examinations of the cost make-up for the Mark III containment shows that a substantial proportion is attributable to the various items required for the interior concrete viz:

	m: million
Stainless steel plate	\$1.2 m
Carbon steel plate (Sacrificial shield)	\$1.0 m
Drywell/Weirwell liners	<u>\$5.2 m</u>

Total 7.4 m, or 20% of the total
cost of the "building structure"

There is no mention of a need for these items in Ref. 2. Conceivably much less expensive plastic coatings could be developed for one or more of these liners. However, there has been no discussion in the published literature of this possibility.

In the Mark III containment of the BWR, the inner shell is in the form of a steel cylinder. This, in turn, is enclosed in an unlined reinforced concrete silo. The reasons for the choice of this arrangement are not clear, and appears to lead to an increase of about 7 percent in the cost of the complete containment system, as compared with the single, MS-lined concrete shell used for the Reference Design of PWR.

In view of the relatively low design pressure (15 psig) and the early removal of a high proportion of the volatiles by the suppression pool, it might be possible to achieve an adequate performance with a single pre-stressed concrete shell, with a plastic liner, and an increase in the capacity of the containment spray system. It is estimated that the overall saving would be about 15 percent of the total cost of the containment system. Similarly, if it were necessary to employ the double containment concept, in order to improve the performance, the Reference Design would be about 15 percent more costly than an arrangement based on two separate plastic-lined, pre-stressed concrete shells.

References of Section 7

- (1) "Capital cost: Boiling Water Reactor plant," NUREG-0242, C00-2477-6, June, 1977.
- (2) L. C. Koke, G. M. Roy and R. E. Brandon, "Optimizing the design of BWR Power Plants," Proceeding American Power Conference, 1972.

8. Discussion of Results

The main points which emerge from this study are discussed in this Section.

8.1 Degree of conservatism in Reg. Guides 1.3 and 1.4

If, as appeared to be the general belief prior to the TMI-2 accident, the most severe accident for which the containment need be designed was a well-conditioned large LOCA, then Reg. Guides 1.3 and 1.4 could be considered to be over-conservative. Since the TMI-2 accident this premise can no longer be sustained, and can now be seen that less severe initiating events can lead to accidents in which the source terms are much larger than had previously been expected.

Moreover, even before the TMI-2 accident, there was a growing appreciation by the electrical generating industry in the U.S. and in other countries that, in the face of mounting public resistance to nuclear power, it could be advantageous to be able to claim that accidents to their reactors would not cause any interruption of the public's activities. To justify this claim for accidents up to, and including, Class 8 would have required a standard of containment comparable with that needed to meet the requirements of Reg. Guides 1.3 and 1.4.

8.2 Scope for re-optimization of containment systems in the U.S.

A simple cost-benefit approach, based on the "expectation of loss" principle, suggests that there should be no need of containments for LWRs. However, it seems so unlikely that this basis for optimization would be acceptable in the U.S. this approach has not been pursued further in this study. In U.S. conditions the cost of the containment system for a 1140 MW(e) PWR is about 10 per cent of the total cost of the plant (i.e. about \$70 million at 1979 prices); however, it should be noted that this figure

includes the cost of all the reactor support and shielding structures. In the U.S., even if there were no requirement for a containment per se, the need to provide a shielding structure proof against earthquakes and tornadoes, together with the other support and shielding roles, would limit the maximum achievable saving to about 50 per cent of the total cost of the containment system, or about 5 per cent of the total plant cost. Thus, an upper limit to the achievable saving is about \$35 million, in 1979 prices.

8.3 Most promising means for reducing containment costs in PWRs

It seems unlikely that overall leak rates of more than 0.1 per cent per day, by volume, will be acceptable. The most promising methods for securing a substantial reduction in cost without sacrificing performance depend on decreasing the design pressure of the primary containment structure and obviating the need for a leak-tight steel liner which does not contribute to the strength of the structure.

The possible configurations which would meet these objectives falls into three classes:

Class I. Use of a steel vessel as the pressure retaining envelope.

Class II. Use of pressure suppression systems to lower the design pressure and to reduce the post-accident working pressure rapidly to such low levels that low leak rates can be achieved without the use of steel liners or

Class III. Use of the double containment concept to reduce the overall leak-rate in systems embodying the features of Class I and/or Class II.

Unfiltered venting of the containment, to reduce the peak pressure, is a possible alternative to pressure suppression in Class II but offers so little additional advantage in cost that it is an unattractive solution.

The alternative objective, that of achieving a higher standard of performance without a significant increase in cost, is more likely to be achieved at minimum cost by further exploitation of the double containment concept, with the aim of using it in the "pump-back" mode rather than in the "extract" mode.

8.4 Summary of results of optimization study for PWR containment

The most promising individual schemes for reduction in cost of containment, without a reduction in performance, are:

- Class I. Use of steel vessel as the pressure retaining envelope. In practice this approach is mainly of value as a basis for a double shell containment system at minimum cost (see below), rather than for saving cost in situations where a single shell system can provide adequate performance. However, the use of the spherical steel shell offers some advantages that cannot be expressed in economic terms, in the form of more space for plant items, at no increase in cost.
- Class II. Pressure suppression systems. Partial exploitation of the concept, as in the Westinghouse ice containment design, leads to a saving of about 15 per cent of the Ref. 1 system cost whilst retaining the conventional MS liner. However, about 40 per cent of this saving is attributable to the reduction in containment vessel dimensions, which introduces economically unquantifiable disadvantages. Full exploitation of the concept, as in the Canadian high dousing rate design, should permit the use of a plastic liner, in conjunction with a pre-stressed concrete shell, giving a saving of about 25 per cent of the Ref. 1 system cost.

Thus the same percentage saving would be achieved as that estimated for the Canadian system, although in the latter case the disadvantages of reduced space are avoided, as it has been assumed that the dimensions of the containment vessel would be the same as in Ref. 1. It should be noted that at the present time it is not entirely clear whether the difficulties encountered with the limited number of ice containments in operating reactors are of a generic nature or are readily remediable design errors. It should be noted that a vented containment system (unfiltered), which can be regarded as a more primitive form of pressure suppression, is also estimated to give a 25 per cent saving, if a plastic liner is used, without reducing the dimensions of the containment vessel.

Of these 3 pressure suppression systems the Canadian one might prove to be the most acceptable, as it provides full retention of the initial containment atmosphere but yields the same saving as the ice condenser without having to reduce the space available for plant items. However, full substantiation of the pressure/time behavior of the system is not readily available in the U.S. at the present time. The use of pumped systems to provide high rate dousing, which would be more in accordance with U.S. practice is estimated to be substantially more expensive than the Canadian gravity fed system and would not be economically viable. One outstanding query to be resolved in relation to the use of pressure suppression systems for PWRs is that of the effect of the reduction in back pressure on the ECCS. However, as the total cost of the Safety Injection System and Residual Heat Removal system is only 15 per cent of the total cost of the containment system, a substantial increase in injection capacity or pumping capacity should not have a major effect on costs.

Class III. Use of double containment concept to improve performance and/or reliability.

In situations where high performance and reliability of performance are required the double containment concept should have a substantial advantage over single shell systems.

The use of a spherical steel shell for the inner vessel, in conjunction with a plastic lined pre-stressed concrete outer shell, should provide this advantage at an insignificant increase in cost, as compared with the Ref. 1 design. Similarly, the use of two plastic lined pre-stressed concrete shells is a possible alternative, but would lead to a significant increase in cost (about 7 per cent). Obviously designs based on the use of a steel lined inner concrete shell and a plastic-lined, outer, prestressed concrete shell would also be satisfactory, but these are estimated to be some 20 per cent more expensive than the Ref. 1 design. It is of interest that, in fact, in a number of plants this additional cost appears to have been incurred, on the grounds that it would provide an additional margin of safety in meeting the requirements of Reg. Guide 1.4.

8.5 Most promising means for reducing containment costs in BWRs.

As all BWR containment systems are based on the pressure suppression concept it follows, from the discussion in Sec. 8.3 above, that there is less scope for re-optimization than in the PWR system. Nevertheless it may be possible to achieve a reduction of 15 per cent of the total cost of the containment system (equivalent to 17 per cent of the total cost of the PWR containment system of Ref. 1), by the use of a single, pre-stressed concrete shell, with plastic liner, and increased containment spray capacity in preference to the double shell arrangement (inner steel shell and outer reinforced concrete silo) of the Ref. 2 design. In situations where a

higher standard of performance and reliability is required a double containment system based on the use of two plastic-lined, pre-stressed concrete shells would cost about the same as the arrangement described in Ref. 2. There is also the possibility of a substantial reduction in cost, up to 15 per cent of that of the total system, if alternative means could be found for protecting the interior concrete structures.

8.6 Recommendations for further work

Although this study has shown that there is scope for changes in existing containment designs which could lead to substantial reductions in cost (up to 25 per cent of total cost of containment system) most of these changes would require further development work. Before embarking on a development program it would be desirable to address two other questions. These are:

- (a) What effect will the TMI-2 accident have on the requirements for containment design, e.g., will there be a requirement to design for accidents more severe than Class 8?
- (b) Is there sufficient incentive for utilities to seek savings of up to 25 per cent of the cost of containment systems (about \$15 million per reactor, at 1979 prices) to justify the cost of the development which would be required?

Some useful work could be carried out in relation to both of these questions, viz: evaluation of the suitability of those systems which offer most promise of a substantial cost reduction as a basis for more accident-resistant designs. Examination of the factors likely to effect utility decisions on containment requirements in the future, including the attitudes of both regulators and utilities to the use of quantified risks as a basis for design decisions. If it appears worthwhile to continue the generic development of containment systems in order to reduce costs and/or improve

their resistance to severe accident conditions, areas which should be given high priority are:

1. Analysis of the relative reliabilities of pressure suppression and pressure-retaining containment systems, in order to confirm that further development of pressure suppression systems will not prove to be abortive on grounds of inadequate reliability.
2. Analysis of the operating and pre-operational experience with ice-containments in U.S. and elsewhere, in order to establish whether the difficulties encountered to date are inherent in the concept or are due to specific and remediable design faults.
3. Analysis of the pressure-time response in the Canadian high dousing rate pressure suppression system, in order to define a program of test work, if this should be necessary.
4. Review of U.S. and other data relating to the leak rates of large pre-stressed concrete shells and review of the effects of plastic liners as a means of reducing the leak rate of such structures, in post-accident conditions, from
 - (a) Single shell pressure suppression containment systems.
 - (b) Inner and outer shells of double pressure retaining containment systems.
5. Examination of possible methods for reducing the cost of protecting interior concrete structures of BWR containments.

In each of these 5 areas the maximum severity of the accident conditions to be designed for would have to be defined at the start of the program. The development of the detailed program of work aimed at cost reduction would also have to take account of the progress of work related to the improvement in resistance to more severe accidents, which it is assumed would be continuing in parallel. Fortunately, one of the more likely additional

requirements, the provision of a post-accident filtered venting system, would be unlikely to inter-act strongly with the most promising methods for cost reduction.

References of Section 8

- (1) "Capital Cost: Pressurized Water Reactor Plant," NUREG-0241, C00-2477-5, June 1977.
- (2) L. C. Koke, G. M. Roy and R. E. Brandon, "Optimizing the design of BWR Power Plants," *Proceeding American Power Conference*, 1972.

9. Conclusions

There is scope for the reduction of containment systems costs of up to 25 per cent in both PWRs and BWRs, without sacrificing present performance standards.

Prior to the TMI-2 accident it might have been argued that the present performance standards were unduly severe, so that somewhat greater savings might have been achieved. This now appears to be unlikely. Further work will be required to establish whether the impact of the TMI-2 accident will prevent the achievement of any significant cost savings. In this context the effects of the TMI-2 accident on both the regulatory body and the utilities have to be taken into account.

Achievement of reductions of more than 2 or 3 per cent of present PWR containment system costs depend on the use of pressure suppression; it is estimated that existing "ice containment" designs provide a 15 per cent reduction. If plastic liners for pre-stressed concrete containment shells could be developed to a point at which they would be acceptable in the U.S., this cost reduction could be increased to about 25 per cent. A similar reduction should be achievable by the use of the Canadian rapid dousing system of pressure suppression in conjunction with plastic lined pre-stressed concrete containment shells. The Canadian system has the advantage that the cost reduction could be achieved without reducing the space available within the containment. If plastic liners became acceptable in the U.S., savings of up to 15 per cent of BWR containment system costs should be possible. Further savings by the use of alternative lining materials for interior concrete are also possible. As an alternative to cost reduction whilst retaining present performance standards, there may also be a demand for higher performance without an increase in cost.

Double containment systems (using plastic lined, pre-stressed concrete outer shells and either similar inner shells or spherical steel shells) should be capable of meeting this requirement, including control of the rare gases emissions, by utilization of the "pump-back" principle. It is possible to define the main elements of the technological development program required to justify changes that would lead to the cost reductions described above. However, whether or not the utilities would have a need for such a program is uncertain at the present time, owing to the effects of the TMI-2 accident. Nevertheless, some specific areas of work to assist in resolving this uncertainty have been identified. Some preliminary work to identify the effects of the TMI-2 accident on containment design requirements has also been identified; as an outcome of this work a need for additional features in the containment design (e.g. a post-accident filtered venting system) may be identified. This preliminary work should also have as one of its objectives, a clear definition of the conditions within the containment on which the design of the system should be based.

Appendix 1. Assumptions used in Reg. Guide 1.4 for evaluating the potential radiological consequences of a loss of coolant accident for pressurized water reactors.

The assumptions related to the release of radioactive material from the fuel and containment are as follows:

(a) Twenty-five percent of the equilibrium radio-active iodine inventory developed from maximum full power operation of the core should be assumed to be immediately available for leakage from the primary reactor containment. Ninety-one percent of this 25% is to be assumed to be in the form of elemental iodine, 5% of this 25% in the form of particulate iodine, and 4% of the 25% in the form of organic iodides.

(b) One-hundred percent of the equilibrium radioactive noble gas inventory developed from the maximum full power operation of the core should be assumed to be immediately available for leakage from the reactor containment.

(c) The effects of radiological decay during holdup in the containment or other buildings should be taken into account.

(d) The reduction in the amount of radioactive material available for leakage to the environment by containment sprays, recirculating filter systems, or other engineered safety features may be taken into account.

(e) The primary containment should be assumed to leak at the leak rate incorporated in the technical specifications for the duration of the accident. The leakage should be assumed to pass directly to the emergency exhaust system without mixing in the surrounding reactor building atmosphere and should be assumed to be released as an elevated plume for those facilities with stacks.

Acceptable assumptions for atmospheric diffusion and dose conversion

are:

(a) The 0-8 hour ground level release concentration may be reduced by a factor ranging from one to a maximum of three (See Fig. A1.1) for additional dispersion produced by the turbulent wake of the reactor building in calculating potential exposures. The volumetric building wake correction, as defined in section 3-3.5.2 of Meteorology and Atomic Energy 1968, should be used only in the 0-8 hour period; it is used with shape factor of 1/2 and the minimum cross-sectional area of the reactor building only.

(b) No correction should be made for depletion of the effluent plume of radioactive iodine due to deposition on the ground, or for the radiological decay of iodine in transit.

(c) The breathing rate of persons offsite should be assumed to be those given in ICRP publication 2, Committee II-1959 as follows:

<u>Time Period</u> <u>(Hours)</u>	<u>Breathing Rates</u> <u>(M³/sec)</u>
0-8	3.47×10^{-4}
8-24	1.75×10^{-4}
24-720	2.32×10^{-4}

(d) The iodine dose conversion factors should be assumed to have the values given in ICRP Publications 2, Report of Committee II, "Permissible Dose for Internal Radiation," 1959. These are as follows:

<u>Iodine Dose Conversion Factors</u>	
<u>Isotope</u>	<u>Rem-Thyroid/Curie Inhaled</u>
I-131	1.48×10^6
I-132	5.35×10^4
I-133	4.00×10^5

<u>Isotope</u>	<u>Rem-Thyroid/Curie Inhaled</u>
I-134	2.50×10^4
I-135	1.25×10^5

(e) External whole body doses should be calculated using "infinite cloud" assumptions i.e., the dimensions of cloud assumed to be large compared to the distance that the gamma rays and beta particles travel. Under these circumstances the rate of energy absorption per unit volume is equal to the rate of energy released per unit volume. For an infinite uniform cloud containing X curies of beta-radioactivity per cubic meter the beta dose in air at the cloud center is:

$${}_B D_{\infty}' = 0.457 \bar{E}_B X$$

The surface body dose rate from beta emitters in the infinite cloud can be approximated as being one-half this amount (i.e., ${}_B D_{\infty}' = 0.23 \bar{E}_B X$). For the gamma emitting material the dose rate in air at the cloud center is

$${}_B D_{\infty}' = 0.507 \bar{E}_\gamma X$$

From a semi-infinite cloud, the gamma dose rate in air is

$${}_Y D_B' = 0.25 \bar{E}_\gamma X$$

(f) The following specific assumptions are acceptable with respect to the radioactive cloud dose calculations:

(1) The dose at any distance from the reactor should be calculated based on the maximum concentration in the plume at that distance, taking into account specific meteorological, topographical, and other characteristics which may affect the maximum plume concentration. In the case of beta radiation, the receptor is assumed to be exposed to an infinite

cloud at the maximum ground level concentration at that distance from the reactor. In the case of gamma radiation, the receptor is assumed to be exposed to only one-half the cloudowing to the presence of the ground. The maximum cloud concentration always should be assumed at ground level.

(2) The appropriate average beta and gamma energies emitted per disintegration, as given in the Table of Isotopes, Sixth Edition, by J. M. Hollander, I. Perlman should be used.

(g) The atmospheric diffusion model should be as follows:

1. The basic equation for atmospheric diffusion from a ground level point source is:

$$X/Q = \frac{1}{\pi \sigma_y \sigma_z \bar{u}}$$

where

X = the short term average centerline value of the ground level concentration (curie/meter³)

Q = amount of material release (curie/sec)

$\bar{u}$ = wind speed (meter/sec)

σ_y = the horizontal standard deviation of the plume (meters)

[See Figure V-1, Page 48, Nuclear Safety, June 1961, Vol. 2., Number 4, "Use of Routine Meteorological Observations for Estimation Atmospheric Dispersion," F. A. Gifford, Jr.]

σ_z = the vertical standard deviation of plume (meters) [See the same reference for σ_y]

2. For time periods of greater than 8 hours the plume should be assumed to meander and spread uniformly over a 22.5° sector.

The resultant equation is

$$x/Q = \frac{2.032}{\sigma_z \bar{u} x}$$

3. The atmospheric diffusion model for an elevated release, as a function of the distance from the reactor, is based on the information in the table below.

<u>Time Following Accident</u>	<u>Atmospheric Conditions</u>
0-8 hours	Pasquill Type F, Wind Speed 1 meter/sec, uniform direction
9-24 hours	Pasquill Type F, Windspeed 1 meter/sec., variable direction within a 22.5 sector.
1-4 hours	a) 40% Pasquill Type D, Windspeed 3 meter/sec. b) 60% Pasquill Type F, Windspeed 2m/sec. c) Wind direction variable within a 22.5 sector.
4-30 days	a) 33.3% Pasquill Type C, Windspeed 3m/sec. b) 33.3% Pasquill Type D, Windspeed 3m/sec. c) 33.3% Pasquill Type F, Windspeed 2m/sec. d) Wind direction 33.3% frequently in a 22.5 sector.

4. Figures A1.2(A) and A1.2(B) give the ground level release atmospheric diffusion factors based on the parameters given in g.3.

Fig. A1.1 Building Wake Dispersion Correction Factors

Fig. A1.2(A) Ground level Release Atmospheric Diffusion Factors For Various times Following Accidents

Fig. A1.2(B) Ground Level Release Atmospheric Diffusion Factors For Various Times Following Accidents

Appendix 2 Accident Assumptions Used in Discussion of Accidents in
Environmental Reports (Reg. Guide 4.2)

Accident 2.0 SMALL RELEASE OUTSIDE CONTAINMENT

These releases shall include such things as releases through steamline relief valves and small spills and leaks of radioactive materials outside containment. These releases shall be included and evaluated under routine releases in accordance with proposed Appendix 1.

Accident 3.0 RADWASTE SYSTEM FAILURE

3.1 Equipment leakage or malfunction (includes operator error)

- (a) Radioactive gases and liquids: 25% of average inventory in the largest storage tank shall be assumed to be released.
- (b) Meteorology assumptions: χ/Q values are to be 1/10 of those given in NRC Safety Guide No. 3 or 4.
- (c) Consequences should be calculated by weighting the effects in different directions by the frequency the wind blows in each direction.

3.2 Release of waste gas storage tank contents (includes failure of release valve and rupture disks)

- (a) 100% of the average tank inventory shall be assumed to be released.
- (b) Meteorology assumptions: χ/Q values shall be 1/10 of those given in Safety Guide No. 3 or 4.
- (c) Consequences should be calculated by weighting the effects in different directions by the frequency

the wind blows in each direction.

3.3 Release of liquid waste storage tank contents

- (a) Radioactive liquids: 100% of the average storage tank inventory shall be assumed to be spilled on the floor of the building.
- (b) Building structure shall be assumed to remain intact.
- (c) Meteorology assumptions: λ/Q values shall be 1/10 of those given in NRC Safety Guide No. 3 or 4.
- (d) Consequences should be calculated by weighting the effects in different directions by the frequency the wind blows in each direction.

Accident 4.0 FISSION PRODUCTS TO PRIMARY SYSTEM (BWR)

4.1 Fuel cladding defects

Release from these events shall be included and evaluated under routine releases in accordance with proposed Appendix 1.

4.2 Off-design transients that induce fuel failures above those expected (such as flow blockage and flux maldistributions).

- (a) 0.02% of the core inventory of noble gases and 0.02% of the core inventory of halogens shall be assumed to be released into the reactor coolant.
- (b) One percent of the halogens in the reactor coolant shall be assumed to be released into the steamline.
- (c) The mechanical vacuum pump shall be assumed to be

automatically isolated by a high radiation signal of the steamline.

- (d) Radioactivity shall be assumed to carry over to the condenser where 10% of the halogens shall be assumed to be available for leakage from the condenser to the environment at 0.5% day for the course of the accident (24 hours).
- (e) Meteorology assumptions χ/Q values shall be 1/10 of those given in NRC Safety Guide No. 3 dated November 2, 1970.
- (f) Consequences should be calculated by weighting the effects in different directions by the frequency the wind blows in each direction.

Accident 5.0 FISSION PRODUCTS TO PRIMARY AND SECONDARY
SYSTEMS (PRESSURIZED WATER REACTOR)

- 5.1 Fuel cladding defects and steam generator leak release from these events shall be included and evaluated under routine releases in accordance with proposed Appendix 1.
- 5.2 Off-design transients that induce fuel failure above those expected and steam generator leak (such as flow blockage and flux maldistributions).
 - (a) 0.02% of the core inventory of noble gases and 0.02% of the core inventory of halogens shall be assumed to be released into the reactor coolant.
 - (b) Average inventory in the primary system prior to the transient shall be based on operation with 0.5% failed fuel.

(c) Secondary system equilibrium radioactivity

prior to the transient shall be based on a 20 gal/day steam generator leak and a 10 gpm blowdown rate.

(d) All noble gases and 0.1% of the halogens in the steam reaching the condenser shall be assumed to be released by the condenser air ejector.

(e) Meteorology assumptions λ/Q values should be 1/10 of those given in NRC Safety Guide No. 4.

(f) Consequences should be calculated by weighting the effects in different directions by the frequency the wind blows in each direction.

5.3 Steam generator tube rupture

(a) 15% of the average inventory of noble gases and halogens in the primary coolant shall be assumed to be released into the secondary coolant.

The average primary coolant activity shall be based on 0.5% failed fuel.

(b) Equilibrium radioactivity prior to rupture shall be based on a 20 gallon per day steam generator leak and a 10 gpm blowdown rate.

(c) All noble gases and 0.1% of the halogens in the steam reaching the condenser shall be assumed to be released by the condenser air ejector.

(d) Meteorology assumptions: λ/Q values shall be 1/10 of those given in NRC Safety Guide No. 4.

(e) Consequences should be calculated by weighing the effects in different directions by the

frequency the wind blows in each direction.

Accident 6.0 Refueling Accidents

6.1 Fuel bundle drop

- (a) The gap activity (noble gases and halogens) in one row of fuel pins shall be assumed to be released into the water. (Gap activity 1% of total activity in a pin.)
- (b) One week decay time before the accident occurs shall be assumed.
- (c) Iodine decontamination factor in water shall be 500.
- (d) Charcoal filter efficiency for iodines shall be 99%.
- (e) A realistic fraction of the containment volume shall be assumed to leak to the atmosphere prior to isolating the containment.
- (f) Meteorology assumptions χ/Q values shall be 1/10 of those given in NRC Safety Guide No. 3 or 4.
- (g) Consequences should be calculated by weighing the effects in different directions by the frequency the wind blows in each direction.

6.2 Heavy object drop onto fuel in core

- (a) The gap activity (noble gases and halogens) in one average fuel assembly shall be assumed to be released into the water. (Gap activity shall be 1% of total activity in a pin.)
- (b) 100 hours of decay time before object is dropped shall be assumed.

- (c) Iodine decontamination factor in water shall be 500.
- (d) Charcoal filter efficiency for iodines shall be 99%.
- (e) A realistic fraction of the containment volume shall be assumed to leak to the atmosphere prior to isolating the containment.
- (f) Meteorological assumptions: λ/Q values shall be 1/10 of those given in NRC Safety Guide No. 3 or 4.
- (g) Consequences should be calculated by weighting the effects in different directions by the frequency the wind blows in each direction.

Accident 7.0 Spent Fuel Handling Accident

7.1 Fuel assembly drop in fuel storage pool.

- (a) The gap activity (noble gases and halogens) in one row of pins shall be assumed to be released into the water. (Gap activity shall be 1% of total activity in a pin.)
- (b) One week decay time before accident occurs shall be assumed.
- (c) Iodine decontamination factor in water shall be 500.
- (d) Charcoal filter efficiency for iodines shall be 99%.
- (e) Meteorology assumptions: λ/Q values shall be 1/10 of those given in NRC Safety Guide No. 3 or 4.
- (f) Consequences shall be calculated by weighting the effects in different directions by the frequency the wind blows in each direction.

7.2 Heavy object drop onto fuel rack

- (a) The gap activity (noble gases and halogens) in one average fuel assembly shall be assumed to be released into the water. (Gap activity is 1% of total activity in a pin.)
- (b) 30 days decay time before the accident occurs shall be assumed.
- (c) Iodine decontamination factor in water shall be 500.
- (d) Charcoal filter efficiency for iodines shall be 99%.
- (e) Meteorology assumptions: χ/Q values shall be 1/10 of those given in NRC Safety Guide No. 3 or 4.
- (f) Consequences should be calculated by weighting the effects in different directions by the frequency the wind blows in each direction.

7.3 Fuel cask drop

- (a) Noble gas gap activity from one fully loaded fuel cask (120-day cooling) shall be assumed to be released. (Gap activity shall be 1% of total activity in the pins.)
- (b) Meteorology assumptions- χ/Q values shall be 1/10 of those given in NRC Safety Guide No. 3 or 4.

(c) Consequences should be calculated by weighting the effects in different directions by the frequency the wind blows in each direction.

Appendix 3. Fission product gas release from thermal reactor fuel

Appendix 3.1 Introduction

The principal objective of defining the source term is to determine the quantity of radiologically significant fission products released from defected LWR fuel rods under accident conditions. One of the fission product release terms for a reactor loss of coolant accident (LOCA) is the rapid escape of gaseous activity that occurs when fuel claddings first rupture. The rapid depressurization of the ruptured rods sweeps out the fission products that have accumulated in the gas gap and the gas plenum during the normal operating period of the fuel rods. This release term is frequently referred to as the gap release component. Analytical techniques have been developed by different laboratories for estimating the magnitude of release. These techniques, which depend upon different mathematical models or different interpretations of experimental release data in making gap release data, have been developed independently by different laboratories as discussed below each section.

Below about $1,900^{\circ}\text{K}$, the main gas release mechanism in UO_2 is a fission gas atom diffusion-like process. However, this process which is temperature dependent does not make a significant contribution at temperatures less than 1300°K . Below that temperature there are two other processes which are of some significance in the surface layer of the fuel and which are temperature independent. These are described as "recoil" and "knock-out" mechanisms and are dependent on the kinetic energy of the fragments formed by fission. However, because of the only limited range of these effects (about $10\text{ }\mu\text{m}$) their contribution to the total fission product release from LWR fuel is negligible. Above about

1900°K, and with high temperature gradients corresponding to oxide fuels, several other release mechanisms, which tend to favor still-higher fractional gas release, begin to dominate. Fission gas bubbles are expected to migrate up the thermal gradient or randomly to either fuel-pin free space directly or to grain boundaries. In addition to this, it has been suggested that above about 1,900°K, pore-sintering and lenticular void migration and grain-boundary sweeping associated with in-reactor fuel restructuring could also release fission gas.

During power transients, significant amounts of fission gas are released caused by thermal-shock induced fuel cracking occurring during fuel cooling accompanying power decreases. But in most of the models described below, which are based on a diffusion process. During normal reactor operation, it is assumed that there is no substantial increase in fuel temperature during accidents.

The various models for the diffusion process and a preliminary empirical model are described in the following sections.

Appendix 3.2. BCL's model (Battelle's Columbus laboratories)⁽¹⁾

In this model, it is assumed that the fission products migrate through the fuel by a diffusion process during reactor operation, and are released to the rod gap and plenum spaces by escape from external surfaces, surface-connected cracks, or surface-connected porosity. The equivalent sphere model originally proposed by Booth⁽²⁾ and discussed by Lustman⁽³⁾ constitutes the basis for calculating release from the sintered UO_2 characteristic of water reactor fuels. The sintered fuel, although near theoretical density, contains some interconnected porosity which effectively increases the surface area for release of fission gases. In the equivalent sphere approach, the fuel body is considered to be composed of spherical particles of uniform size whose surface to volume ratio is equivalent to the actual surface area to fuel volume ratio of sintered material. Solutions for the appropriate diffusion equations for a sphere in which production and decay of the diffusion product are taken into account have been obtained by Beck⁽⁴⁾ and these are used directly in the REGAP computer code developed at Battelle-Columbus.

The differential equation which describes the diffusion gas from a sphere is

$$\frac{\partial C}{\partial t} = \frac{D}{r} \frac{\partial^2 (rc)}{\partial r^2} + B - \lambda C \quad (1)$$

With boundary conditions: $C(a,t) = \text{finite}$

$$C(a,t) = 0$$

initial condition: $C(r,0) = 0$

where

C = concentration in the sphere, atoms per cm^3 .

D = diffusion coefficient, cm^2 per sec.

B = production rate, atoms/sec/cm³.

λ = decay constant, sec⁻¹.

t = time, sec.

r = radial coordinate in the sphere, cm.

a = equivalent-sphere radius, cm.

The rate of release (R) is found from the concentration gradient at the outer surface and for a unit volume of the solid is given by

$$R = -\frac{3D}{a} \left(\frac{\partial C}{\partial r} \right)_{r=a} \quad (2)$$

The accumulation of a fission product external to the solid can be calculated by considering the rate at which it is released and the rate at which it decays:

$$\frac{dN}{dt} = R - \lambda N \quad (3)$$

where N is the accumulation of undecayed release atoms from a unit volume of sphere (atoms per cm³). Beck⁽⁴⁾ restated the problem in terms of dimensionless variables and obtained the following solution:

$$G = 3 \left(\frac{1}{\sqrt{u}} \coth \sqrt{u} - \frac{1}{u} \right) [1 - \exp(-u\zeta)] - \frac{6u \exp(u\zeta)}{\pi^2} \sum_{n=1}^{\infty} \frac{1 - \exp(-n^2 \frac{2\pi^2 \zeta}{\pi^2 + u})}{n^2 (\pi^2 + u)} \quad (4)$$

where

$G = \frac{N}{B/\lambda}$ = the ratio of non-decayed atoms outside the sphere at any time to the total non-decayed atoms in the system at equilibrium.

$$u = \frac{\lambda a^2}{D}$$

$$\zeta = \frac{Dt}{a^2}$$

$$D = D_0 \exp(-Q/RT)$$

D_0 = limiting diffusion coefficient, cm^2 per sec.

Q = activation energy for diffusion, cal per g-mole.

R = gas constant, cal/(deg) (g-mole)

T = absolute temperature, deg K.

For reactor operating times that are long with respect to the half-life of a radioactive fission product, G approaches the equilibrium value:

$$G_{\infty} = 3 \left[(1/u) - \coth \sqrt{u} - 1/u \right] \quad (5)$$

For stable fission-product species, the release is expressed in terms of the ratio of the amount released to the amount produced. Thus,

$$G/F = \frac{N}{Rt} = 1 - \frac{1}{15\zeta} + \frac{6}{\pi^4\zeta} \sum_{n=1}^{\infty} \frac{\exp(-n^2\pi^2\zeta)}{n^4} \quad (6)$$

The first five terms of the summation are usually sufficient to obtain an accurate value for G .

The diffusion coefficients D are obtained from the Arrhenius expressions as a function of temperature by specifying the limiting diffusion coefficient D_0 and the activation energy for diffusion. Of course, these values are different for each fission product chemical element. Extensive experimental work has been done over the past two decades, mainly directed at the noble gas elements krypton and xenon, to determine the fundamental D_0 and Q values for UO_2 fuel. Most of the work has involved post-irradiation laboratory heating experiments although some effort has been spent on extracting diffusion coefficients from the results of inpile UO_2 irradiation studies. In this model, the basic diffusion parameter data were obtained from fission product release results reported by Parker, et al.⁽⁵⁾ for trace irradiated UO_2 .

The fuel temperatures that are needed for the calculation of

diffusion coefficients are obtained by solving a one-dimensional radial heat conduction equation for a representative set of fuel pin positions.

A series of calculations was performed in which the values of various parameters were changed to examine the effect on release predictions. The ranges of variations of parameters were considered to represent the approximate degree of uncertainty in the available input data although the variation in D_0 values could also be interpreted as an estimate of possible effect of fuel burnup on the release rates. The best estimate values for the series of parameters are considered to be the following:

(a) Gap conductance = $1000 \text{ Btu/hr-ft}^2\text{-}^\circ\text{F}$.

(b) UO_2 thermal conductivity = $1.2 \text{ Btu/hr-ft-}^\circ\text{F}$.

(c) Equivalent sphere radius = 0.00613 cm .

(d) The D_0 parameter = The values in Table 10 of reference 6.

The fission product release results obtained using the REGAP code with best estimate parameters are listed in Table A3.1 & A3.2.

Appendix 3.3 ANC's model (Aerojet Nuclear Company)⁽⁷⁾

The methods used here are based on a concentration equation for fission product diffusion which assumes that the driving force for diffusion of fission products is the gradient of the chemical potential of the diffusion particles.⁽⁸⁾ W. Yuill has incorporated this idea into his models for calculating fission production diffusion.⁽⁹⁻¹²⁾

The concentration balance equation in cylindrical coordinate is^(B)

$$\frac{dc}{dt} = P(r) - \lambda C(r,t) + \frac{1}{r} \frac{d}{dr} r \frac{DC}{RT} \frac{du}{dr} \quad (7)$$

where

$C(r,t)$ = concentration at radial position r at time t of some particular fission product (mols/cc).

$P(r)$ = production of the fission product at r (mols/cc-sec.)

λ = decay constant for the fission product

D = Fick's law diffusion constant for the product (cm^2/sec)

$u = \left(\frac{\partial G}{\partial C} \right)_T$ = chemical potential for the fission product. The G is the Gibbs free energy [Cal/mol/cc].

Evaluation of the expression $(DC/RT) \left(\frac{du}{dr} \right)$ which appears in equation (7) necessitates some approximation. First it is assumed that it may be approximated by the ideal equation.

$$u = u^0(T) + RT \ln C. \quad (8)$$

The equation for diffusion current is

$$J = -D' \frac{du}{dr} = -D' \left[\left(\frac{\partial u}{\partial T} \right)_C \frac{dT}{dr} + \left(\frac{\partial u}{\partial C} \right) \frac{dC}{dr} \right] \quad (9)$$

If there were no temperature gradients, this would reduce to Fick's law. Using $(\partial u/\partial c)_T$ from equation (8), this yields

$$D' = \frac{DC}{RT}.$$

To apply equation (7) requires a knowledge of $(\partial u/\partial T)_c$, D and dT/dr throughout the pin. The temperature gradient dT/dr can be calculated from a knowledge of the heat generation within the pin and the heat conductivity $kc(T)$ as a function of temperature. This is readily accomplished if it is assumed that kc depends only on temperature and is not seriously affected by such structural changes as may occur within the UO_2 pellet due to the marked temperature changes between the boundary and interior.

The diffusion coefficient D is assumed to have a temperature dependence given by

$$D = D_0 \exp(-Q/RT) \quad (10)$$

Data on D_0 and Q for noble gases are incorporated into FPFM code. ⁽¹²⁾

Data for $(\partial u/\partial T)_c$ are available only for C_s and Sr ⁽¹¹⁾. Because of this the releases of noble gases and their iodine precursors are calculated using a correlation which is partly based on equation (7) and partly empirical.

In calculating release of long-lived noble gases and iodines, Yuill, et. al. ⁽¹¹⁾ developed equations based on the steady state form of equation (7), with $dC/dt = 0$. For stable gases ($\lambda = 0$) they cast their equation into the form ⁽¹²⁾.

$$\ln(1-F) = - \frac{e^{-Q/RT_b}}{b^2 T_b} e^{(A+BG+CG^2)} \quad (11)$$

where F = release fraction

b = pellet radius (cm)

T_b = boundary temperature of pellet ($^{\circ}\text{K}$)

Q = activation energy for Fickian diffusion as in equation (10)

$$G = \text{heat rating} = \int_{T_b}^{T_0} k_c dT = \frac{\bar{P}_z}{4\pi}$$

$\bar{P}_z$ = watts/cm in pin

k_c = heat conductivity (watts/cm. $^{\circ}\text{C}$)

A , B , C are constants derived from analysis of capsule data. In cylindrical coordinate, the heat conduction equation is

$$\frac{\partial}{\partial r} \left(r k_c \frac{\partial T}{\partial r} \right) = - \frac{T_b}{\pi b^2} \psi(r) \times r \quad (12)$$

Where $\bar{P}_z$ is the average linear rating (watts/cm) and $\psi(r)$ is a function to take account of the variation in production rate with position r .

Thus in order to apply equation (11) to an actual reactor account must be taken of the lower distribution.

Yuill has shown that the release fractions for short-lived isotopes is proportional to $1/\lambda$. Following Yuill's suggestion, the assumption has been made that the release of ^{131}I is the same as for its ^{131}Xe daughter. Then the release of all other noble gases and iodines with half lives shorter than 8.05d are calculated from

$$F_m = F_{\text{Xe}}^{131} \frac{\lambda I^{131}}{\lambda_m} = F_{\text{Xe}}^{131} \frac{\tau_m}{\tau_{I^{131}}} \quad (13)$$

In equation (13) t_1^{131} and t_m refer to the half lives of I^{131} and the species m , respectively. The equation is applied for noble gases or iodines for which $t_0 < t_1^{131}$.

The best estimate data on D_0 , $(\frac{\partial u}{\partial T})_c$, and Q are from the Table X, reference 11.

$$D_0 = 8.53 \times 10^{-9} \text{ cm}^2/\text{sec.}$$

$$(\frac{\partial u}{\partial T})_c = 5 \text{ cal/mol-deg.}$$

$$Q = 6100 \text{ cal/mol.}$$

The fission product releases based on ANC's analytical model with the specified input data, (7) describing the PWR and BWR operating conditions, are listed in table A3.1 & A3.2.

Appendix 3.4 Parker's model⁽¹³⁾ (ORNL)

In order to formulate a direct determination of gap release of the various fission products, extensive use was made of both the experimentally determined relative diffusion rates for different elements from early ORNL work, as well as an empirically determined absolute relation between temperature and the diffusion parameter D'_i (D' prime, empirical) by Lorenz at ORNL (Ref. 14) based on a series of Canadian Capsule experiments in which only xenon and krypton were measured. The ORNL data, like most experimental data, are derived from so called annealing type experiments which must be critically evaluated for the initial burst contribution as has been done by Oi and Tajake (Ref. 15) and by Lorenz and Creek (Ref. 16).

Percent release vs. temperature values were taken initially from Fig. 6.3 of ORNL Report 3981 (Ref. 17) and the release values were used to compute D' (D' -prime) values according to a method originally derived by Booth and Rymer⁽¹⁸⁾. They write:

$$F = 1 - \frac{6}{\pi^2} \sum_{n=1}^{\infty} \frac{1}{n^2} \exp(-\pi^2 n^2 D' t) \quad (14)$$

where

F = fraction release

t = time in seconds at temperature.

Reliable approximations of equation (14) are possible.

(1) when $F \geq 0.77$

$$F = 1 - \frac{6}{\pi^2} e^{-\pi^2 D' t} \quad (15)$$

(2) when $F < 0.77$

$$F = 6(D' t / \pi)^{1/2} - 3D' t \quad (16)$$

The computed D' values were plotted as D' versus $10^4/T$ and fitted to a straight line by the method of least squares. This gave the expression:

$$\ln D' = a + B(10^4/T) \quad (17)$$

or

$$D' = Ae^{B(10^4/T)} \quad (18)$$

where T = absolute temperature and $\ln(A) = a$. The constant are listed in Table A3.5. The diffusion parameters (D 's) were also adjusted for burn-up by a correction, K , as follows:

$$D' = D'_0 \exp\left[2.303 \left(\frac{MWD/T}{25000} - 1\right)\right] = D'_0 K \quad (19)$$

where D'_0 = diffusion coefficient calculated from the reference experimental data,

(MWD/T)	K
5,000	0.158
15,000	0.398
25,000	1.000

The D 's derived from the ORNL experimental data were empirically normalized to those for 25,000 MWD/T burnup, by comparison of the xenon-krypton data from numerous other measurements (Refs. 19,20,21) of the relation between gaseous release and burnup at constant temperature.

A simple computer code, so called DIREL⁽¹³⁾, summarizes the fractional release of twenty principal nuclides using the thermal profile predicted by the thermal analysis code "Little Mamu".⁽¹³⁾ In addition use has been made in DIREL of the following approximations:

(a) The maximum fission product inventory that a reactor may have should reoccur at the end of any equilibrium cycle. In a typical third-year fuel replacement scheme, the average time of irradiation would be

two cycles or about 650 days. This value affects mainly the longlived or stable nuclides.

(b) The average burnups for fuel in the outer, middle, and center thirds of a reactor core are assumed to be about 5000, 15,000, and 25,000 MWD/T, respectively, at the end of an equilibrium cycle.

(c) Diffusion coefficients for diffusion products increase by a factor of 10 every 15,000 MWD/T burnup (Ref. 6).

(d) Thermal conductance across the cladding gap is assumed to range between 500 and 1000 BTU/hr/pt². The outer one third and half of the center one third are assumed to be unrestructured and to conform to the lower conductance.

For thermal analysis, a fuel rod is divided into nine equal lengths. Seven temperature values are given for seven points along the fuel radius of each length. In the gap release computer program DIREL: (1) a D-prime for each activity is calculated for each of the seven temperatures using equation (18); (2) F (fractional release) values, at equilibrium, are computed using the D' values according to:

$$F = 3(WD'/\lambda)^{1/2} \quad (20)$$

when $T \leq 400^{\circ}\text{C}$, $F = D$ and $W =$ correction factors. If λ is less than 2×10^{-8} , then the isotope is considered to be stable and F is computed according to

$$F = 4(WD't/\pi)^{1/2} \quad (21)$$

(3) the seven values of "F" are averaged; (4) steps 1 and 2 are repeated for remaining eight lengths of fuel rod in axial directions; (5) the nine "average F" values are averaged to produce the "fuel rod release"; and (6) the average release is computed for the eight regions specified and

then

$$\text{Total release} = \sum_{i=1}^8 (\%) \text{ release}_i \times \text{VF}_i \times \text{PF}_i \quad (22)$$

where VF = volume fraction and PF = power factor.

The final release estimates, including effects of the gap release coefficient escape fraction are listed in table A3.1 & A3.2.

Appendix 3.5 R. A. Lorenz, et. al's model (ORNL)⁽²²⁾

Experiments performed at Oak Ridge National Laboratory with fission product simulants and with irradiated light-water reactor (LWR) fuel have been employed to develop semi-empirical models of fission product release from the defected LWR fuel rods. This model has been developed for estimating the source term models required for analysis of cesium and iodine release during a PWR-LOCA.

The models for fission product release in steam are based on tests conducted over the temperature range 500(^oC) to 1200 (^oC). One series of tests, the Implant Test Series⁽²³⁻²⁵⁾, employed simulated fission products which were coated on unirradiated UO₂ fuel pellets; a second series, the Low Burnup Fuel Test Series⁽²⁶⁾, used fuel capsules irradiated to 1000 MWD/T at high heat-rating (560 to 660 w/cm); and a third series of experiments, the High Burnup Test Series⁽²⁷⁻³¹⁾, used fuel irradiated to 30,000 MWD/T in the H.B. Robinson reactor at low-heat rating (175 to 320 w/cm).

The models assume that the release is the sum of two components: burst release (that carried out with escaping plenum gas when the rod ruptures), and diffusional release (that diffusing from gap space after the plenum gas has vented).

The mass of cesium which escapes as "burst release" was found to be equal to the volume of plenum gas vented, multiplied by the concentration of cesium in vapor form. Cesium concentration in the vented gas has been determined to be a function of absolute temperature and the total inventory of cesium in the gap space as estimated from multiplying the radioactive inventory source by the gap release fraction.

In the temperature range 973 to 1173°K, cesium burst release can be expressed as

$$M_B = V_B \frac{G^{0.8}}{7.74} \exp(6.06 - \frac{7420}{T}) \quad (23)$$

where

M_B = mass of cesium released in the burst, μ_g Cs,

V_B = volume of plenum gas vented, cm^3 at 0°C and system pressures

G = inventory of cesium in the pellet-cladding gap per unit area of cladding, μ_g Cs/ cm^2 , and

T = temperature at rupture location, K.

A simple depletion equation is used to model cesium released by diffusion in steam over the temperature range 500 to 1200°C,

$$M_D = M_0 [1 - \exp(-\frac{R_0 t}{M_0})] \quad (24)$$

where

M_D = mass of cesium released by diffusion, μ_g Cs,

M_0 = total calculated mass of cesium initially in the gap, μ_g Cs,

t = time at diffusion temperature, hr, and

R_0 = initial rate of release of cesium by diffusion, μ_g Cs/hr.

The initial rate of release by diffusion, R_0 in turn, is given by

$$R_0 = \left[\frac{W}{200} \right] \left[\frac{G^{0.8}}{7.74} \right] \left[\frac{0.101}{P} \right] \left[\exp \left(19.96 - \frac{19,810}{T} \right) \right] \quad (25)$$

where

W = width of radial gap, μm , and

P = system pressure, MPa.

The model for release of iodine follows the same pattern as for cesium. The iodine burst release in the temperature range 973 to 1173°K is expressed as

$$M_B = V_B \left[\frac{G^{0.8}}{1.15} \right] \exp(1.09 - \frac{3770}{T}) \quad (26)$$

The release of iodine by diffusion in the temperature range 500 to 1200 °C is given by Eq. (24), but Ro for iodine is given by

$$Ro = \left[\frac{W}{200} \right] \left[\frac{G^{0.8}}{1.15} \right] \left[\frac{0.101}{P} \right] \exp(15.3 - \frac{14,800}{T}) \quad (27)$$

These models were applied to the analysis of a PWR LOCA in which the reactor characteristics were assumed as follows: 33,000 fuel rods, each containing 2500 g of UO₂ and operated so that equal numbers of rods had average burnups of 10,000, 20,000, and 30,000 MWD/T. The fuel rods were divided into six groups, each containing the amount of released fission gas shown in table A3.4. Since the fuel rods would rupture near their centers, the cesium and iodine gap inventories were based in peak burnups estimated to be 10% higher than the above average burnups. It was further assumed that all fuel rods ruptured and experienced a temperature transient to 1200°C for an effective time of 10 minutes.

The diffusion equation⁽¹⁾ predicts that the fractional release of radioactive isotopes from the fuel to the plenum and void spaces will be less than for stable isotopes. For small fractional releases, typical of the PWR taken as an example (but not typical of the WASH-1400 PWR), the reduction is inversely proportional to the square root of the decay constant for isotopes reaching production-decay equilibrium. The magnitude of the reduction also depends upon the irradiation time, but release by knockout is essentially independent of half-life.

This model is believed to satisfactorily predict "best-estimate" cesium and iodine release from full length fuel rods in steam provided

the range of temperatures, times, and gap inventories employed in the tests is not exceeded. This model employed for the estimation of gap inventories is only tentative and has not been verified fully by testing of irradiated fuel rods. But in view of lack of experimental confirmation, it would be advisable to use these results only as a lower limit.

Also this ORNL model does not include the recent interesting phenomenon like iodine spiking which may have a significant effect on offsite thyroid dose resulting from postulated accidents involving the release of reactor coolant. There is only a limited amount of data relating to this phenomenon. However, an approximate estimate of iodine spiking effect for PWR plant can be made in Table A3.5 by using the data of W. F. Pasedag's report⁽³²⁾ and his correlation. From this calculation, the iodine release due to iodine spiking for a large PWR plant (1000 MWE power output) is about 10,000 (Ci). This number is about 30 percent of the iodine source term of the recent ORNL model. Thus the ORNL model for iodine release has been adjusted by a 30 percent addition due to iodine spiking. A similar iodine spiking estimate for BWR plant is given at Table A3.6 by using the data of W. F. Pasedag's report and his correlation.

It is of interest to compare these release values with those used in WASH-1400. Values for gap inventory and gap escape fraction (the fraction of gap inventory which escapes following rupture) obtained by using these models are listed in Table A3.7. These "best-estimate" predictions of total cesium and iodine release differ from those calculated in WASH-1400 by factors of 200 and 60, respectively. These large differences are due to two factors: a lower estimate for initial fission

gas inventory in the gap than used in WASH-1400, and lower estimates of gap escape fractions.

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Table A3.1 Fractional Released to Sep. (Total Core)

Fission Product (decay half-life)	Calculated Fractions			Chemical Groups	Average Release Fraction
	ANC (a)	BCL (b)	ORNL (c)		
Xe, Kr (long lived)	0.06	0.10	0.08	Noble Gases	0.03 ^(d)
Xe-133 (5.27 day)	0.04	0.02	0.02		
Xe-135 (9.2 hour)	0.0002	0.004	0.004		
I, Br (long lived)	0.06	0.10	0.14	Halogens	0.05 ^(d)
I-131 (8.06 day)	0.06	0.03	0.05		
I-132 (2.3 hour)	0.0007	0.005	0.006		
I-133 (20.8 hour)	0.007	0.01	0.02		
Cs, Rb (long lived)	0.20	0.05	0.21	Alkali Metals	0.15 ^(e)
Cs-138 (32 minutes)	0.00001	0.0005	0.005		
Sr, Ba (long lived)	0.000004 ^(f)	0.02	0.02	Alkaline Earths	0.01 ^(d)
Sr-89 (51 day)	-	0.01	0.015		
Sr-91 (9.7 hour)	-	0.002	0.01		
Te-132 (78 hour)		(estimated value)		Tellurium	0.10 ^(d)

(a) ANC (Aerojet Nuclear Company) model.

(b) BCL (Battelle's Columbus laboratories) model.

(c) ORNL (Oak Ridge National Laboratory) model.

(d) Values can be higher or lower by a factor of 4.

(e) Values can be higher by a factor of 2 or lower by a factor of 4.

(f) This value results from thermodynamic restrictions not considered in the other two models. See discussion of the escape fraction for this species.

Table A3.2 Gap Release Component Values

Fission Product Species	Gap Release Fraction	Gap Escape Fraction	Total Gap Release Value
Xe, Kr	0.03 ^(a)	1	0.03
I-Br	0.05 ^(a)	1/3 ^(c)	0.017
Cs, Rb	0.15 ^(b)	1/3 ^(c)	0.05
Sr, Ba	0.01 ^(a)	10 ⁻⁴ (d)	0.000001
Te, Se, Sb	0.10 ^(a)	10 ⁻³ (d)	0.0001
Others	-	-	Negligible ^(e)

(a) Values can be higher or lower by a factor of 4.

(b) Value can be higher by a factor of 2 or lower by a factor of 4.

(c) Values can be higher or lower by a factor of 3.

(d) Values can be higher or lower by a factor of 100.

(e) While no numerical value was developed for these various species, the number should not exceed that used for strontium-barium.

Table A3.3 Values For Constants in the Arrhenius Equation

Radioelement	Constant A	Constant B
Xenon-krypton	232.3	-4.00534
Iodine	2.936	-2.95177
Cesium	1.369	-2.62966
Strontium	3.37 x 10 ¹⁵	-11.2368
Ruthenium	3.8495	-7.01379
Tellurium	53.27	-3.3510

Table A3.4. Summary of calculated Fission Product Release from example PWR.

Number of rods in group ^a	Fission gas in plenum (% of total in rod)	Cesium and iodine in gap space (% of total in rod)	Total released from rod group	
			g Cs	g I
390	8	19.65	1.80	0.38
1,760	4	9.65	7.52	1.48
5,970	2	4.65	8.90	1.73
10,350	1	2.13	8.10	1.59
7,920	0.5	0.88	3.01	0.59
5,610	0.25	0.25	0.78	0.15
33,000	-	-	30.11 ^b	5.92 ^c

^aAn equal number of rods in each group has inventories corresponding to average burnups of 10,000, 20,000, and 30,000 MWD/MT.

^b80% released at time of rupture; 20% released by diffusion.

^c72% released at time of rupture; 28% released by diffusion.

Table A3.5 Iodine spiking data for PWR plants

plant name	design power(MWe)	Total equilibrium release rate ($\mu\text{Ci/sec}$)	Equilibrium release rate per power ($\mu\text{Ci/MWe/sec}$)
Ginna	490	364	0.743
Haddam	575	242	0.421
Pt. Beach 1	497	287	0.577
Main Yankee	790	1285	1.626
O'Conee	819	680	0.830

Table A3.6 Iodine spiking data for BWR plants

plant name	design power(MWe)	Total equilibrium release rate ($\mu\text{Ci/sec}$)	Equilibrium release rate per power ($\mu\text{Ci/MWe/sec}$)
Dresden 2	809	70	0.0865
Oyster Creek	640	85	0.133
9 Mile Rt.	625	65	0.104
Millstone Rt. 1	652	43	0.066

Table A3.7 Comparison of WASH-1400 and ORNL Model Calculations

Element	Gap inventory ^a (% of total inventory)		Gap escape fraction (% of gap inventory)		Total Release (% of total inventory)	
	WASH-1400	Model	WASH-1400	Model	WASH-1400	Model
Xe and Kr	3	1.27	100	100 ^b	3	1.27 ^b
Cs	15	2.79	33	0.89	5	0.025
I	5	2.79	33	1.91	1.4	0.053

(A) Calculated for stable and long half-life isotopes. The gap inventory and total release of ^{131}I and ^{133}Xe would be 3 to 9 times lower.

(b) An additional amount of fission gas, approximately 1.5% of the total inventory, would be released during heated up.

Appendix 4. Nature of Realistic leakage Calculation

Appendix 4.1 Introduction

The nature of the leakage, including the equations that define its rate as a function of its driving forces, is dependent upon a number of factors. The major factors of importance are whether or not the critical pressure is exceeded, the diameter, length, and roughness or tortuosity of the leakage path, and the mean free path and viscosity of the escaping gas.

A brief review of the parameters that govern the flow characteristic of a gas passing through a small restriction will help to clarify the problem of leakage behavior. Experimental determinations of the flow rate of gases through leaks have been made by Nerken⁽¹⁾ for the size of leaks of interest in vacuum work and leak detection. Since some leaks of interest in containment structures approach the size of leaks investigated by Nerken, the results of his experiments and comparison with theoretical equations can be applied to evaluate the magnitude and significance of leakage. These investigations emphasize, however, the fact that the effects of pressure on the flow rate of leakage (at atmospheric pressure) are not always simply apparent, because a given leak can exhibit several types of flow.

For the particular characteristic geometry of a leak in a vessel, it is desirable to recognize the type of flow that may occur. Since, in turn, it defines the expected leakage rate in terms of the following controlling conditions:

- (1) Geometry of the leak and roughness of passage
- (2) Pressure difference across the leak
- (3) Physical properties of the gas (i.e., viscosity, molecular weight, density)
- (4) Composition of gaseous mixture (air-steam mixtures).

In formulating the leakage parameters for each type of flow, leakage from a containment structure is preferably expressed as the fraction (or percentage) by weight of the contained atmosphere escaping per unit of time. Accordingly, all equations that define flow rate will be expressed in terms of the weight of gas escaping through the leaks.

Appendix 4.2 Molecular flow: The molecular flow regime is defined by mean free path, i.e., as the average distance that a gas molecule will travel between collisions with another molecule. The mean free path varies inversely as a linear function of the pressure. Molecular flow occurs when the mean free path of the gas exceeds the largest dimension of the cross-sectional flow area of the leak passage. Under this condition the molecules will collide more frequently with the walls of the leak passage than with each other. At atmospheric pressure, the mean free path of air is exceptionally small. The average value of mean free path of air molecules⁽³⁾ at standard conditions of 1 atmosphere and 70° F is of 2.6×10^{-6} inch and decreases to approximately 2.6×10^{-7} inch at 10 atm. Molecular flow occurs when the mean free path of the gas exceeds the largest dimension of the cross-sectional flow

area of the leak passage. Under this condition the molecules will collide more frequently with the walls of the leak passage than with each other.

This mean free path of air may be compared with the size of the leaks that exist in a containment wall. The smallest dimensions of leak size that is significant with respect to flow rate is considered to be approximately 1×10^{-4} inch. This might be compared with a leak size of 1×10^{-2} inch, which under constant pressure of 40 psig, would leak at a rate of 0.1% per day based on orifice flow leakage from a containment structure with a free containment volume of 2.5×10^6 ft³. Hence, single leaks below 1×10^{-4} inch diameter do not contribute significantly to the loss of the containment atmosphere.

Obviously, molecular flow does not play a major role in contributing to containment leakage because of the exceedingly small leak dimensions required in this flow regime. However, molecular flow may occur in portions of irregular leak passages where considerable dimensional changes occur, such as in tight cracks in weld seams. If a substantial number of these small leaks existed in a welded containment structure, some slight contribution to the overall leakage might be expected as a result of molecular flow. In the field of vacuum technology, a convenient parameter frequently used to determine the type of flow is the Knudsen number, a/λ , where a is the radius of the leak with a circular cross section, and λ is the mean free path of gas molecules flowing through the leak passage. This parameter, as discussed by

by Dong and Bromley⁽²⁾, affords a convenient means to arbitrarily divide the flow regimes into the various types of flows that characterize the gradual transition between molecular and laminar flow.

The flow regimes have arbitrarily been divided into a range expressed by the value of the Knudsen number:

Viscous (or laminar flow)	$a/\lambda > 10^2$
Slip flows	$10^2 > a/\lambda > 10$
Transition flow	$10 > a/\lambda > 10^{-1}$
Molecular flow	$a/\lambda > 10^{-1}$

To provide a more comprehensive exposition of other parameters that are important in the molecular flow regime and to enable comparisons to be made with other types of flows, the molecular flow equation for the simplest of leak geometry (circular section) is selected. The molecular flow equation was originally derived by Knudsen (and discussed by Dong and Bromley⁽²⁾ and by Dushman⁽³⁾, for a finite cylindrical leak path; in the case of air flow from a higher pressure, P_a (containment pressure), to atmosphere, P_o , this equation is

$$W = K_1' \frac{D^3}{L} \frac{P_a - P_o}{(MRT)^{1/2}} \quad (1)$$

where W = mass flow rate in terms of weight of gas flowing through

leak per unit of time (lbm-ft/sec),

D = diameter of circular cross-section leak (ft).

L = length of leak path (between pressure zones P_a and P_o) (ft).

M = molecular weight of gas,

R = gas constant

T = temperature of gas,

K_1' = the constant of proportionality,

Equation (1) may be further simplified by expressing the containment pressure as a ratio of the downstream absolute pressure at the end of the leak path, which is equal to atmospheric (barometric) pressure

$$K = K_1'' \frac{D^3}{L} \frac{\bar{P}_a - 1}{(MRT)^{1/2}} \quad (2)$$

where $\bar{P}_a$ is the ratio of the upstream absolute pressure to the atmospheric pressure.

The leakage rate, L_r , of the system is the fractional loss of containment atmosphere during time Δt :

$$L_r = \frac{\frac{W_0 - W_1}{W_0}}{\Delta t} \quad (3)$$

where W_0 = weight of gas in the free volume of containment vessel at initial selected state,

W_1 = weight of gas in free volume of containment remaining after the loss sustained through leaks for the selected interval of time.

The most commonly selected time interval, Δt , is 24 hr. Since the containment atmosphere weight difference $(W_0 - W_1)/\Delta t$ is the weight loss per interval of time, or the flow rate of escaping gas, w , the leakage rate can be expressed as:

$$L_r = \frac{w}{W_0} \quad (4)$$

The initial weight of contained atmosphere W_0 , is determined from the equation of state for a perfect gas, assuming uniform conditions throughout the containment volume (not necessarily valid for noncirculated atmosphere):

$$W_0 = \frac{P_a V}{RT} \quad (5)$$

By substitution, the leakage rate, L_r , is then conveniently expressed in terms of defined parameters:

$$L_r = W \frac{RT}{P_a V} \quad (6)$$

In order to find leak rate as a function of pressure for molecular flow, substitute equation (2) into equation (6).

$$\begin{aligned} L_r &= K_1 \frac{D^3 (\bar{P}_a - 1)}{L (MRT)^{1/2}} \frac{RT}{P_a V} \\ &= K_m \left(1 - \frac{1}{\bar{P}_a}\right) \end{aligned} \quad (7)$$

Where K_m = dimensionless constant in leakage rate for molecular flow. The dependence of leakage rate upon the absolute air pressure in the containment vessel for molecular flow is shown in Figure A4.1.

Appendix 4.3 Laminar flow

For gas flowing through leaks at low velocities and with a low differential pressure across the leak, the flow may be considered as incompressible. However, with increased velocities, long leak paths, and significant measure differentials, the gas density is no longer constant between the entrance and the exit of the leak passage. Then the Hagen-Poiseuille equation (4), (5), for the viscous flow of a gas through a straight leak passage of circular section must be modified to treat the flow of a compressible fluid with its accompanying density changes. By substituting the average density of the gas flowing through the leak into the Hagen-Poiseuille equation, the laminar flow equation for a compressible fluid becomes

$$W = \frac{g \pi D^4}{128 \mu L} P_{av} (P_a - P_o) \quad (8)$$

where μ = gas viscosity

However, for the condition at the leak P_{av} may be approximated (T constant) by

$$P_{av} = \frac{P_{av}}{RT} = \frac{P_a + P_o}{2} \times \frac{1}{RT} \quad (9)$$

and thus

$$W = \frac{g \pi D^4}{256 \mu L} \times \frac{1}{RT} \times (P_a^2 - P_o^2) \quad (10)$$

Equation (10) is in the form commonly referred to as the Darcy formula.

A typical application of this equation by Shapiro⁽⁶⁾ is illustrated by a problem of seepage of gas through a tube, this condition may be similar to a leak in containment vessels.

In terms of leak area, A, and a constant of proportionality, K_2 , the equation (10) becomes

$$W = K_2' \frac{AD^2}{\mu L} \times \frac{1}{RT} \times (P_a^2 - P_o^2) \quad (11)$$

To further simplify eq. (11), the pressure P_a may be expressed in terms of atmospheric pressure P_o

$$W = K_2' \frac{AD^2}{\mu L} \times \frac{1}{RT} \times (\bar{P}_a^2 - 1) \quad (12)$$

and $\bar{P}_a$ is written for P_a/P_o .

The leakage rate, L_r , for laminar flow can be obtained by substituting equation (12) into equation (6).

$$\begin{aligned} L_r &= K_2' \frac{AD^2}{\mu L} \frac{1}{RT} \times (\bar{P}_a^2 - 1) \times \frac{RT}{P_a V} \\ &= K_L \left(\bar{P}_a^2 - \frac{1}{\bar{P}_a} \right) \end{aligned} \quad (13)$$

Where K_L = dimensionless constant in the leakage rate for laminar flow.

The dependence of leakage rate upon the absolute air pressure in the containment vessel for laminar flow is shown in Figure A.4.2.

Appendix 4.4 Turbulent Flow

Although the majority of significant leaks in a containment vessel may well be in the laminar flow regime at the pressures which are of interest, it is conceivable that turbulent flow conditions will be attained in some cases, particularly if the leak passages are irregular. The region of transition between laminar flow and turbulent flow may be conservatively estimated to begin with a Reynolds number, Re , greater than 2000. If the leak path in a containment structure is of a nature to permit the development of turbulent flow, the mass flow rate, W , may be derived from the Darcy-Weisbach equation⁽⁷⁾, assuming that the leak cross section approaches the geometry of a small diameter tube. The equation commonly appears as

$$P_a - P_o = f \frac{L}{D} \times \frac{V^2}{2} \quad (14)$$

where f is friction factor whose value is a function of the leak passage roughness and the Reynolds number for the flow conditions through the leak.

A gas flowing through a leak passage where L/D and ΔP may be appreciable would not only encounter wall friction losses but also would expand. Although under such conditions, the behavior may change from incompressible to compressible flow, an approximation may be made by treating the problem as incompressible and assigning average values for the variables in Eq. (14). Thus,

$$P_a - P_o = f \frac{L}{D} \frac{P_{av} V_s^2}{2} \quad (15)$$

where

$$P_{av} = \frac{P_a + P_o}{2} \times \frac{1}{RT(g)},$$

and

$$V_s = \frac{W}{P_{av} g A}$$

which, when substituted in Eq. (15), yield

$$W = g A \left[\frac{D}{f L} \times \frac{1}{RT} \times (P_a^2 - P_o^2) \right] \quad (16)$$

or, if pressures P_a is expressed in terms of atmospheric pressure P_o ,

$$W = K_3 A \left[\frac{D}{f L} \times \frac{1}{RT} (\bar{P}_a^2 - 1) \right]^{1/2} \quad (17)$$

For flows with Reynolds Numbers 4×10^3 to 10^5 , it may be assumed that the profile of flow through the leak approaches that of a smooth tube for which the empirical expression of Blasius' law, ⁽⁸⁾ may be applied in the determination of a conservative friction factor. For circular leak paths,

$$f = \frac{0.0791}{Re^{1/4}} = \frac{0.0791}{\left(\frac{v D \rho}{\mu} \right)^{1/4}} \quad (18)$$

and for noncircular leak paths,

$$f = \frac{0.0791}{\left(\frac{4 v R_H \rho}{\mu} \right)^{1/4}} \quad (19)$$

Where hydraulic radius R_H is defined by $R_H = \frac{2S}{Z}$, S = the flow area, Z = wetted perimeter of flow. Substituting Eq. (18) in the Darcy-Weisbach equation (16) and rearranging terms yields

$$W = K_4 A \frac{D^{5/7}}{\mu^{1/7}} \times \frac{1}{(LRT)^{4/7}} \times (P_a^2 - P_o^2) \quad (20)$$

or, if pressure P_a is expressed in terms of atmospheric pressure P_o ,

$$W = K_4 A \frac{D^{5/7}}{\mu^{1/7}} \left(\frac{\bar{P}_a^2 - 1}{LRT} \right) \quad (21)$$

The relationship defined by Eqs. (17) and (21) should be recognized as indicative of the influence of pressure, temperature, and viscosity on

turbulent flow through an idealized leak of circular cross-section. It is inconceivable that many leaks existing in containment vessels would have smooth, straight surfaces and a uniform internal cross-section for the entire length of the flow path. Instead, it would be more likely that the leak would have an irregular surface and variable flow area. Notwithstanding, the above equation (21) serves to define, in a general way, the more important parameters affecting the leakage rate.

The leakage rate, L_p , for turbulent flow through a rough passage can be obtained by substituting Eq. (17) into Eq. (6):

$$L_p = \bar{w} \frac{RT}{\bar{p}_a V} = K_3 A \frac{D}{f l} \left(\frac{1}{RT} (\bar{p}_a^2 - 1) \right)^{1/2} \frac{RT}{\bar{p}_a V} \\ = K_{tr} \left(1 - \frac{1}{\bar{p}_a^2} \right)^{1/2} \quad (22)$$

Also, the leakage rate, L_p , for turbulent flow through a smooth passage can be obtained by substituting Eq. (21) into Eq. (6):

$$L_p = \frac{RT}{\bar{p}_a V} \bar{w} = K_4 A \frac{D^{5/7}}{u^{1/7}} (\bar{p}_a^2 - 1)^{4/7} \frac{RT}{\bar{p}_a V} \\ = K_{ts} \left(1 - \frac{1}{\bar{p}_a^2} \right)^{4/7} \bar{p}_a^{1/7} \quad (23)$$

Where K_{tr} and K_{ts} are dimensionless constants for turbulent flow through rough passages and smooth passages respectively. The dependence of leakage rate upon the absolute air pressure in a containment vessel for turbulent flow through rough passages and smooth passages are shown in Figure A.4.3 and A.4.4 respectively.

Appendix 4.5 Compressible flow through convergent passages or orifices

Leaks with convergent passages may be considered as nozzles, while leaks whose length is relatively short compared with the diameter may be considered as orifices. Significant changes in gas density occur with changes in velocity in the case of compressible flow through nozzles and orifices that involve the thermodynamic effects of the compressibility of the gas. The flow behavior depends on the velocity attained in the gas stream through the leak and the corresponding sonic velocity of the gas at the flow conditions.

For air, the sonic velocity is given by the relation,

$$C = 49.02T^{1/2} \quad (34)$$

It is noted that the containment atmosphere in the event of a *loss-of-coolant accident*, is essentially a steam air mixture. In such a case, the sonic velocity will be modified both by the temperature and the composition of containment atmosphere. The value of the Mach number, Ma , that determines the effect of downstream pressure on flow will be correspondingly modified.

When a compressible flow is at subsonic velocity, $Ma < 1$, the flow through a leak is always influenced by the downstream pressure conditions. On the other hand, at velocities $Ma > 1$, the downstream pressure conditions cannot affect the leakage, assuming orifice geometries exist. The pressures in a containment structure are, respectively, the inside pressure, P_a , and the outside ambient atmospheric pressure, P_o . At flows corresponding to $Ma=1$ (sonic flow), the P_o/P_a becomes to the critical pressure ratio, which, for air at normal conditions with $\gamma (=c_p/c_v)=1.4$, attains a value of 0.528. (For saturated steam, the critical pressure

ratio is 0.578 which gives an indication that an air-stream mixture would have an intermediate value).

The variation of the mass flow rate, $\dot{W}$ through an orifice-type leak can be determined by applying the compressible flow equation for one-dimensional adiabatic flow ⁽²⁾:

Case 1: For $P_o/P_a < 0.528$ and $P_a < 27.8$ psia, when $P_o = 14.7$ psia,

$$\dot{W} = C_d A \left\{ \frac{P_a^2}{RT} \left[\frac{2\gamma}{\gamma-1} \right] \left[1 - \left(\frac{P_o}{P_a} \right)^{(\gamma-1)/\gamma} \right] \right\}^{1/2} \frac{P_o}{P_a} \quad (25)$$

Case 2: For $P_o/P_a < 0.528$ and $P_a > 27.8$ psia, when $P_o = 14.7$ psia,

$$\dot{W} = C_d A \left(\frac{P_a^2}{RT} \left[\frac{2\gamma}{\gamma-1} \right] \right)^{1/2} \quad (26)$$

Thus, any leakage that is below sonic velocity (containment pressure less than 27.8 psia) is dependent upon both upstream (P_a) and downstream (P_o) pressures, while, leakage that attains sonic velocity (containment pressure greater than 27.8 psia) is independent of the downstream pressure (P_o). Equations (25) and (26) may be expressed in terms of atmospheric pressure and a constant of proportionality. For Case 1:

$$\dot{W} = K_S A \left[\frac{1}{RT} \times \frac{2\gamma}{\gamma-1} \left(1 - \frac{1}{\bar{P}_a^{(\gamma-1)/\gamma}} \right) \right]^{1/2} \bar{P}_a^{(\gamma-1)/\gamma} C_p$$

and for Case 2:

$$\dot{W} = K_S A \left(\frac{1}{RT} \times \frac{2\gamma}{\gamma-1} \right)^{1/2} \left(\frac{2}{\gamma-1} \right)^{1/(\gamma-1)} \bar{P}_a C_v$$

Since the orifice leak geometry is indeterminate for the majority of such leaks in containment structures, the value of the coefficient of discharge C_d can only be estimated. The range of this variation is approximately 0.55 to 0.99 from the experimental work of Grace and Lapple ⁽¹⁰⁾; it is dependent upon the specific orifice geometry. For an

assumed orifice flow behavior of leaks in a containment structure, the discharge coefficient that is applicable is, at best only a guess because of the many undefined orifice leak geometries possible in such structures.

The leak rate, L_r , for compressible flow through orifice, when $\bar{P}_a < 1.9$ and $\gamma = 1.4$, can be obtained by substituting eq. (27), into eq. (6)

$$\begin{aligned} L_r &= K \frac{RT}{\bar{P}_a V} = K_0 A \frac{1}{RT} \left(1 - \frac{1}{\bar{P}_a^{0.286}} \right) \frac{\bar{P}_a^{0.286} RT}{\bar{P}_a V} \\ &= K_0 \left(1 - \frac{1}{\bar{P}_a^{0.286}} \right)^{1/2} \frac{1}{\bar{P}_a^{0.715}} \end{aligned} \quad (29)$$

Also, the leakage rate, L_r , when $\bar{P}_a \geq 1.9$ and $\gamma = 1.4$, is

$$L_r = 0.259 \times K_0 \quad (30)$$

From equation (30), orifice flow in the sonic velocity range is independent of the pressure. The dependence for leakage rate upon the absolute air pressure in the containment vessel for compressible flow through orifice is shown in Figure A.4.5.

To compare the relative magnitude of air flow through containment leak paths, as governed by the relationships of equations (7), (15), (22), (23), and (26) for the flow regimes investigated, it is assumed that a containment structure contains a number of leak paths whose total flow area is equivalent to that of an orifice having a diameter of 0.12 inch (0.01 ft.). The total flow, w , through the leak paths is calculated on the basis of the pounds of air escaping per day from the containment vessel as a function of the containment pressure. It is further assumed that there are sufficient paths of each type to ensure the same total flow area. The resulting Fig. A.4.6 clearly shows that the leak paths which behave as orifices will allow the largest flow of air when the

other flows are initiated on the basis of small areas.

With the knowledge of the parameters that control the leakage in a containment structure, as derived from the relationship in each flow regime investigated, it would then be desirable to know whether containment leakages manifest any predominant leakage characteristic. Only limited experience and leakage tests are available from which justifiable and reliable deductions can be made.

Although for many reported containment leakage tests, it has been assumed that compressible flow orifice is representative of leakage behavior, no available test data or evidence justify the assumption or support this conclusion. Each containment structure should preferably be considered as uniquely influenced by several or all of the following factors that ultimately determine the leakage rate:

- 1). The pressure and temperature conditions of the containment atmosphere.
- 2). The composition and physical properties of the containment atmosphere (in the case of accident conditions).
- 3). The number of potential leakage paths.
- 4). The dissimilarity of flow characteristics among the existing leak paths.
- 5). The variation of leak geometry with variations of internal containment measures.
- 6). The extent of the containment boundary limits beyond the containment shell.

From a practical standpoint, the containment total leakage should be recognized as merely representative of a composite average of the above-mentioned effects, and no attempt should be made to analyze the

leakage dependence of the individual contributing factors.

In practice there is no means of determining, prior to construction, what proportion of the leakage will be attributable to each type of flow. Consequently, it has to be assumed in design that the relationship between leak rate and pressure will be the least favorable (i.e. orifice flow). It will be seen from Fig. A.4.7 that the leak rate for orifice flow would not be changed until it reaches the critical pressure (≈ 1.9 atm.) and would be reduced by 50 percent when the absolute pressure has fallen to 1.1 atm. from an initial value of about 5 atm.; whereas if the flow were entirely laminar flow, it would have fallen to 50 percent at about 2.7 atm. Using the data given by Bechtel in Ref. 11 for the pressure response vs. time relationship for a typical "dry" containment, it was found that the average leak rate over the first 24 hours is 84.3 percent of the initial value (with assumption that all the leakage is due to orifice flow); the rate at 24 hours is about 75 percent of the initial value.

Fig. A4.1 Pressure Relationship to Leakage for Molecular Flow

Fig. A.4.2. Pressure Relationship to leakage for Laminar (viscous) Flow

Fig. A.4.5 Pressure Relationship to Leakage for turbulent (Rough Passage) Flow

Fig. A4.4 Pressure Relationship to Leakage for turbulent (Smooth passage) flow

Fig. A.4.5 Pressure Relationship to Leakage for orifice flow

Fig. A.4.6 Comparative Flow through Leak Paths Based on Assumptions of Equal Area at a Selected Containment Pressure

Fig. A.4.7 Leak Rate vs. Containment Pressure for various flow regimes

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Appendix 5. Theoretical and experimental models of spray washout
of airborne containments.

Appendix 5.1 Introduction

To assess the external hazard it is necessary to estimate the amount of any particular isotope which might be released to the containment atmosphere and the rate of leakage of the containment contents to the outside. Much of the various fission products under accident conditions might deposit or condense in the cooler parts of the reactor circuit, but with gases and low boiling-point substances such as iodine, it is possible that the majority of the release will reach the containment boundary.

Spray systems are installed in the containment vessels of water reactors to reduce containment pressure, remove heat from the containment and remove fission products which could be released from the core as a result of a postulated design basis loss of coolant accident (LOCA).

The maximum thyroid dose which an individual could receive following LOCA is reduced by engineered safety features such as spray systems and by plate-out on containment surfaces. The extent to which potential doses are reduced by a spray system depends on how fast airborne fission products are washed from the containment atmosphere.

As early as 1965, several reactor designs were proposed ⁽¹⁾⁽²⁾⁽³⁾⁽⁴⁾ in which containment sprays were designed to remove both heat and airborne fission products from the containment atmosphere in the

unlikely event of a loss of coolant accident. Spray systems represent a class of gas scrubbers. Aqueous scrubbers similar to containment sprays are widely used in industrial applications and have been studied extensively over a period of more than 40 years. (5), (6), (7), (8)

It is important to recognize that there are differences as well as similarities between industrial scrubbers and containment building sprays. These are: (1). In a typical industrial scrubber the gas and liquid are in countercurrent flow and each makes one pass through the device, whereas in the containment building the same body of air remains in the building for the entire time. (2). In the industrial absorber the contact time for gas is normally in the range of a few seconds, whereas in the containment building the contact time between gas and liquid is comparatively long and the liquid is recycled continuously. (3). In the containment building the mass of material to be absorbed is relatively small, and one can easily afford to use overwhelming excess of chemical reagents to assure complete absorption. (4). In the containment building there are some problems unique to highly radioactive environments, such as radiolysis of the spray solution.

As applications of sprays have developed, spray solutions of several chemical compositions have been used. These include boric acid, boric acid made basic with sodium hydroxide (caustic spray), sodium hydroxide/sodium thiosulfate, and boric acid containing hydrazine at trace level concentrations.

Iodine washout by sprays is complicated by the fact that it may exist in three states: elemental iodine, organic iodides, and particulate iodine. The most persistent of these forms, organic iodides, was shown by Eggleton and Alkins in 1964⁽⁹⁾ to be composed mainly of methyl iodide. The fractional abundance of each of these three forms would vary with accident conditions. For a site suitability source term released into the containment vessel, the percent of each of three species of iodine is conservatively estimated to be 91:4:5.⁽²⁴⁾

Appendix 5.2 Models for spray washout⁽¹⁰⁾

Fission product washout in a containment vessel can be described mathematically by a first order differential equation which equates the rate of the mass accumulation in the containment atmosphere to the difference between source and removal rates:

$$\frac{dC_i}{dt} = G_i - R_{ij}C_i \quad (1)$$

where V = volume of gas space, m^3

C_i = concentration of i th fission product in gas phase,
Kg/ m^3

G_i = i th fission product input rate, Kg/hr

$R_{ij} = j^{th}$ removal rate constant for the i^{th} fission product
(R_{ij} is assumed to be first order with respect to
concentration), m^3/hr .

For realistic calculations both time-dependent source and

removal terms should be considered in modelling the spatial and temporal fission product distributions in multicompartment containment buildings. However, for a conservative analysis of the design basis LOCA an instantaneous source term may be assumed. For an instantaneous source, the time-dependent generation terms, G_i , in eq. (1) as replaced by an initial condition on C_i at time zero. The washout equation may then be written as

$$\frac{dC_i}{dt} = -\sum_{ij} \lambda_{ij} C_i \quad (2)$$

where $\lambda_{ij} = \frac{R_{ij}}{V}$ = the j^{th} removal rate constant for the i^{th} fission product specie, hr^{-1} .

The first order removal process defined in Eq. (2) applies until equilibrium between fission product concentration in the containment atmosphere and the containment liquid mass is approached. At this point Eq. (2) is no longer valid and $\frac{dc}{dt}$ approaches zero. A simple and conservative method of handling the approach to equilibrium is to use the first order washout equation until a cut-off concentration level is reached. After the cut-off concentration level is reached, washout is neglected.

The washout model based on an instantaneous source term and a cut-off concentration to account for equilibrium effects may be obtained by integrating eq. (2) with respect to time:

$$C_i = C_{i0} e^{-\lambda_i t} \quad (3)$$

where $\lambda_i = \sum \lambda_{ij}$

$$C_{i0} = C_i \text{ at time } = 0$$

and imposing the condition $\lambda_i = 0$, for $C_i < C_i$ cut-off. In eq. (3)

λ_i is the total removal constant resulting from all removal mechanisms for the i^{th} fission product specie.

Each fission product specie would have its own λ_i and cut off concentration level. Consequently, eq. (3) must be solved for each iodine specie independently. The following sections describe the calculational methods used to predict $\lambda_{i,s}$ for particular containment systems and fission product species.

Appendix 5.3 Models for absorptions of elemental iodine

Elemental iodine is the dominant iodine specie in post-accident atmospheres. It is removed at an appreciable rate by both spray drop absorption and by wall deposition. For simplicity, subscripts indicating the iodine species are dropped in the development of equation in this section.

Appendix 5.3.1 Absorption by Spray Drops

The removal rate of any absorbable specie may be equated to the product of concentration increase and spray flow rate:

$$R_c = F(C_{eo} - C_{ei}) \quad (4)$$

where R_c = removal rate, Kg/hr = RC

C = airborne iodine concentration, Kg/m³.

R = spray removal rate constant, m³/hr,

F = spray flow rate, m³/hr

C_{eo} = concentration of solute in spray drops leaving atmosphere, Kg/m³,

C_{ei} = concentration of solute in liquid entering atmosphere Kg/m³.

For spray liquid which has not been exposed to the containment atmosphere previously (i.e. during the injection phase of spray operation), $C_{e,i} = 0$

and C_{eo} may be related to the equilibrium partition coefficient, H , though the use of an absorption efficiency, E , defined by:

$$C_{eo} = HE(C) \quad (5)$$

where H = equilibrium partition coefficient applicable to spray absorption.

E = fractional approach to equilibrium by spray drops during a single pass,

C = concentration of solute in gas phase, kg/m^3 .

Eq. (4) and (5) may be used together with eq. (2) to related λ to the spray parameters:

$$\lambda_s = \frac{FHE}{V} \quad (6)$$

where λ_s = washout λ due to the spray, hr^{-1} .

V = volume of sprayed region, m^3 .

The absorption efficiency, E , may be computed by several models. The models differ in degrees of conservatism and simplicity, and hence represent alternatives useful in specific applications.

Stagnant film model

The stagnant film model is based on absorption through both a gas film and liquid film. This model is conservative because it neglects mixing within the drop. In this model, E is computed by

$$E = 1 - \exp - \left[\frac{6k_g t_e}{d(H + \frac{k_g}{k_e})} \right] \quad (7)$$

where k_g = gas film mass transfer coefficient, (m/sec).

t_e = drop exposure time, sec.

d = drop diameter, cm.

k_e = liquid film mass transfer coefficient, cm/sec.

The gas phase mass transfer coefficient, k_g , may be computed from the Ranz-Marshall approximation⁽¹¹⁾ to the Frossling equation⁽¹²⁾:

$$k_g = \frac{D}{d} (2 + 0.6 Re^{0.5} Sc^{0.33}) \quad (8)$$

where D = diffusivity of solute in gas phase, cm^2/sec .

Re = Reynolds number for fall drop.

Sc = Schmidt number for solute in gas phase.

On the liquid side of the drop interface, the film coefficient is predicted by Griffiths'⁽¹³⁾ approximation to the rigid drop⁽¹⁴⁾ absorption equation:

$$k_e = \frac{2Dl}{3d} \quad (9)$$

where D = diffusivity of solute in liquid, cm^2/sec .

Eq. (9) is a conservative formulation of the rigid drop equation in that it predicts lower rates of absorption than the rigid drop equation.⁽¹⁵⁾

Rigid drop model

The rigid drop model is similar to the stagnant film model in that it represents a mathematical solution to absorption by a stagnant sphere, accounting for gas phase and liquid phase mass transfer resistance. Dankwert's⁽¹⁴⁾ equation for drop absorption can be written in terms of the absorption efficiency, E , as⁽¹⁵⁾:

$$E = 1 - \sum_{n=1}^{\infty} \frac{6 S_h^2 \exp(-n^2 \theta)}{n^2 [\alpha_n^2 + S_h (S_h - 1)]} \quad (10)$$

$$S_h = \frac{k_g A}{H D_e}$$

$$\theta = \frac{D_e t_e}{a^2}$$

a = drop radius, cm.

$$\alpha_n = N^{\text{th}} \text{ root of } \alpha_n \cot \alpha_n + (S_h - 1) = 0$$

Numerical comparisons of the stagnant film and rigid drop models⁽¹⁵⁾ show that the stagnant film model typically under predicts the E values as compared to the rigid model by less than 16%.

Well-mixed drop model

In the well-mixed drop model, mass transfer resistance inside the drop is neglected. The solute concentration on the liquid side of the gas/liquid interface is equated to the average concentration in the drop. While the liquid phase mass transfer resistance is neglected, gas/liquid equilibrium are properly accounted for in this model. The drop efficiency may be expressed as⁽¹⁵⁾

$$E = 1 - \exp \left[-\frac{6k_g t_e}{dH} \right] \quad (11)$$

By comparison with the stagnant film E, Eq. (7), Eq. (11) would apply where k_e was large (small drops or circulating drops) or where H was large compared to k_g/k_e (reactive liquid).

Numerical values of E predicted from eq. (11) are as much a factor of two higher than values of E predicted from stagnation film model.

For H values larger than 10^5 , all three models yield similar predictions.

<Appendix 5.3.2> Deposition of iodine on interior containment surfaces

Surface deposition of iodine occurs as the result of several transport processes which occur in series. Regimes of transport include: the bulk gas phase, the gas boundary layer, the liquid film, and the solid wall surface. Of these, transport in the gas boundary layer has been shown to be the controlling step.⁽¹⁶⁾

Several models have been proposed to describe fission product removal due to surface deposition. Of these, the Knudsen-Hilliard⁽¹⁷⁾

and the Yuille-Baston⁽¹⁸⁾ model appear to be in good agreement with available experiments. The Knudsen-Hilliard model views deposition as a gas film transport process to vessel surfaces. The gas film mass transfer coefficient is predicted from natural convection heat transfer correlations by a mass-heat transfer analogy. The Yuille-Baston model is based on the penetration theory for mass transfer, and uses a natural convection heat transfer model to estimate gas flow velocities.

In the Knudsen-Hilliard model, the bulk gas in the containment atmosphere is assumed to be well-mixed by natural convection, by steam flows, and by spray operation. A gas boundary layer is established adjacent to containment surfaces. For laminar boundary layer the mass transfer coefficient across the gas boundary layer is predicted by

the equation

$$\frac{k_g L}{D} = 0.59 (\text{GrSc})^{1/4} \quad (12)$$

where

k_g = film mass transfer coefficient

L = length measured along deposition surface

D = diffusivity of iodine in gas phase

Gr = Grashov number

Sc = Schmidt number

For turbulent boundary flow, the mass transfer coefficient is predicted using the equation

$$\frac{k_g L}{D} = 0.13 (\text{GrSc})^{1/3} \quad (13)$$

The transition from laminar flow to turbulent flow occurs at a critical Grashov number. Important variables in Grashov number include length L , and temperature difference between the gas and the wall surface.

Therefore the plate length at which the flow transition occurs depends on thermal conditions in the containment vessel.

The iodine removal rate constant for a particular compartment in the containment is given by

$$\lambda_n = \frac{k_g A}{V} \quad (14)$$

where λ_n = removal rate constant due to surface deposition

k_g = average mass transfer coefficient

A = surface area for wall deposition

V = volume of contained gas

Appendix 5.3.3 overall absorption rate

The overall rate is the sum of that due to spray operation and natural convection plate-out:

$$\lambda = \lambda_s + \lambda_n \quad (15)$$

Thus the airborne concentration at any time, t , after the release of an instantaneous source term is:

$$C = C_0 e^{-(\lambda_s + \lambda_n)t} \quad (16)$$

Appendix 5.4 Models for absorption of organic iodides

Methyl iodide is slightly soluble in water, and reacts slowly with pure water. Therefore its absorption rate is governed by mass transfer resistance in the liquid phase. This is different from elemental iodine which is more soluble in water, and undergoes very rapid hydrolysis through ionic reactions.

For slightly soluble gases which are relatively inert chemically, the absorption rate is strongly influenced by the presence of reactive additives which destroy the dissolved solute. Models for the absorption rates of drops, wall films, and sump pools will be described in detail.

<Appendix 5.4.1> Absorption by a stagnant drop

Absorption by a stagnant drop with a surface resistance may be predicted from a solution of the diffusion equation written in spherical coordinates

$$\frac{\partial C}{\partial t} = \frac{D}{r^2} \left[\frac{\partial}{\partial r} \left(r^2 \frac{\partial C}{\partial r} \right) \right] - R \quad (17)$$

subject to the surface condition

$$D \frac{\partial C}{\partial N} = k_g (C_g - C_{g1}) \quad (18)$$

where N = outward directed normal at the surface of the drop.

D = diffusivity in the liquid,

R = destruction rate per unit volume by chemical reaction.

A solution to eq. (17), subject to the boundary condition (18) and assuming C is initially zero within the drop, has been given by Danckwerts.⁽¹⁴⁾ The total amount of absorption in time t is obtained by integrating the flux over the area and time. The final equation for the amount of solute gas absorbed by a drop is

$$Q = 8\pi h^2 C_g Da^2 \sum_{n=1}^{\infty} \frac{kt(k + D\alpha_n^2) - D\alpha_n^2 (\exp[-t(k + D\alpha_n^2)] - 1)}{(k + D\alpha_n^2)^2 [a\alpha_n^2 + h(ah - 1)]} \quad (19)$$

where $h = \frac{k_g}{HD}$, cm^{-1}

C_g^* = HC_g = solute concentration on liquid side of interface, K_g/cm^3 ,

α_n = N^{th} root of $(a\alpha) \cot(a\alpha) + ah - 1 = 0$, cm^{-1}

k = first order reaction rate constant, sec^{-1}

t = exposure time, sec.

a = drop radius, cm

D = diffusivity of solute in liquid, cm^2/sec .

Q = total amount of solute absorbed during exposure time t , k_g .

The boundary condition, eq. (19), has been assumed to apply at all points on the surface of the drop. Also the reaction rate, R, was taken to be that for a first order reaction, kC.

If gas phase resistance is small, h becomes large. In the limit as h approaches infinity, eq. (20) becomes

$$Q \approx 8\pi a D C^* \sum_{n=1}^{\infty} \frac{k a^2 + \frac{D n^2 \pi^2}{a^2} \{1 - \exp[-t(k + \frac{D n^2 \pi^2}{a^2})]\}}{k a^2 + D n^2 \pi^2} \quad (20)$$

This equation would be expected to apply for absorption of slightly soluble substances such as methyl iodide unless the reaction rate constant k, were very large. For very large values of the reaction rate gas phase resistance is applicable.

Both eq. (19) and eq. (20) can be written in terms of absorption efficiency, E. Since E is equal to the attained solute concentration divided by the equilibrium concentration, $E = \frac{3Q}{4\pi a^3 C^*}$.

<Appendix 5.4.2> Wall film absorption⁽¹⁰⁾

The flow characteristics and absorption by liquid films have been extensively studied during the past 25 years. Based on theoretical and experimental studies, the general characteristics of wetted wall flow may be stated as follows. At low flow velocities, laminar flow persists, the velocity profile is parabolic, and the free surface velocity is 3/2 the average velocity. At Reynolds numbers in the range 5-25, waves begin to appear on the surface, though the flow is substantially laminar. At Reynolds number of 250-500 the flow becomes turbulent.

Absorption into a wetted wall film is described mathematically by eq. (21):

$$\frac{\partial C}{\partial t} + V_x \frac{\partial C}{\partial x} + V_y \frac{\partial C}{\partial y} + V_z \frac{\partial C}{\partial z} = D \left(\frac{\partial^2 C}{\partial x^2} + \frac{\partial^2 C}{\partial y^2} + \frac{\partial^2 C}{\partial z^2} \right) - R \quad (21)$$

where C = concentration of solute in liquid, K_g/m^3 .

t = time, sec.

V_x, V_y, V_z = fluid velocities in x, y, z directions, m/sec.

D = diffusion coefficient of solute in liquid, m^2/sec .

R = destruction rate per unit volume by chemical reaction = kC .

$k_g/m^3\text{-sec}$,

k = first order reaction rate constant, sec^{-1}

The total absorption rate for methyl iodine is obtained by integrating the absorption rate per unit area over the surface area. Analytical solution is possible only for relative simple flow regimes. A simple flow regime of practical use is that corresponding to fully developed one dimensional flow. For this case, the velocity profile can be written

$$V_z = V_{\max} \left[1 - \left(\frac{x}{\delta} \right)^2 \right] \quad x \leq \delta \quad (22a)$$

$$V_x = 0 \quad (22b)$$

$$V_y = 0 \quad (22c)$$

Where $V_{\max}$ = velocity at gas/liquid interface,

z = distance parallel to plane of film, measured in direction of liquid flow,

x = distance measured perpendicular to plane of film

measured from surface of film toward solid surface,

δ = thickness of liquid film.

Using this velocity profile, and neglecting diffusion in the Y -direction eq. (21) becomes

$$V_{\max} \left[1 - \left(\frac{x}{\delta} \right)^2 \right] \frac{\partial C}{\partial z} = D \frac{\partial^2 C}{\partial x^2} - kC \quad (23)$$

For short length of laminar film, the solute does not have time to penetrate far into the film, and hence absorption takes place as though the film were infinite in thickness. The differential equation for this case involving the penetration theory approximation, is obtained from eq. (23) by setting x equal to zero.

$$V_{\max} \frac{\partial C}{\partial z} = D \frac{\partial^2 C}{\partial x^2} - kC \quad (24)$$

Eq. (24) may be written in terms of exposure time,

$$t = \frac{\delta^2}{V_{\max}} \quad (25)$$

The solution to eq. (24) is given by Danckwerts (20) as

$$q = C^* \frac{D}{k} \left[(kt + 1/2) \cdot \text{erf}(kt) + \frac{kt}{\pi} e^{-kt} \right] \quad (26)$$

where C^* = concentration in liquid at the gas-liquid interface

q = amount absorbed per unit area up to time t .

Most experiment data obtained for laminar flow in short wetted wall columns agree with prediction based on the penetration theory. Serious discrepancies however may be encountered if the absorption process causes interfacial turbulence. ⁽¹⁹⁾ Interfacial turbulence enhances absorption compared to predictions based on the penetration theory.

<Appendix 5.4.3> Absorption by sump pool liquid⁽¹⁰⁾

Uptake of methyl iodide in sumps in a PWR containment system can be important compared to spray and wall film absorption for spray solutions which react slowly with methyl iodide. For the slow reaction regime, exposure of liquid in the form of sprays and wall films merely cause it to attain saturation with respect to the gas phase. If no additional reaction occurred, recirculated liquid would re-enter saturated in methyl

iodide, and no further absorption would occur. However, the residence time of liquid in the pool is relatively long, permitting relatively slow reactions to destroy absorbed methyl iodide.

A material balance made on the spray liquid can be used to relate the methyl iodide washout rate to the pool reaction rate. For the pool, the following CH_3I material balance can be written

$$\text{Input rate} = FHC_g \quad (27a)$$

$$\text{Output rate} = FC_l + kV_l C_l \quad (27b)$$

$$\text{Accumulation rate} = 0 \text{ for steady state absorption } (27c)$$

and

$$FHC_g = FC_l + kV_l C_l \quad (27d)$$

where

F = volumetric spray flow rate entering pool, m^3/sec .

k = reaction rate constant, sec^{-1}

C_l = CH_3I concentration in pool liquid, kg/m^3 ,

V_l = volume of pool liquid, m^3 .

A similar material balance made for the gas phase leads to

$$FC_g = FHC_g + V_g \left(\frac{dC_g}{dt} \right) \quad (28)$$

If eq. (28) is solved for C_l , the methyl iodide concentration in the liquid, and the result substituted into eq. (27) and the resulting equation solved for C_g , the result is

$$C_g = C_{g0} \exp - \left[\frac{FkHt}{V_g(F/V_l + k)} \right]$$

where C_{g0} = airborne methyl iodide concentration at time zero, kg/m^3 ,

F = spray flow rate, m^3/sec ,

V_g = volume of gas phase, m^3 ,

V_L = volume of liquid, in containment pump, m^3 ,

k = first order reaction rate constant, sec^{-1}

Eq. (29) would be applicable for cases where k is too low to appreciably enhance absorption during a single pass of spray drops or wall films through the containment atmosphere. This case obtains for water sprays which contain no special additive for methyl iodide, hence would apply to boric acid, buffered boric acid, and sprays made basic with sodium hydroxide.

<Appendix 5.4.4> Effect of spray solution composition

Pure water reacts very slowly with methyl iodide, and methyl iodide is only slightly soluble in water. Therefore, the spray removal rate is small unless reactive chemicals are added to the spray solution.

Methyl iodide reacts in aqueous solution by substitution reactions (26) in which a nucleophilic agent replaces the methyl radical. Schwendiman et. al.⁽²⁰⁾ have summarized reaction rates available prior to 1968. These are reproduced in Table A5.1.

Of the reactants listed in Table A5.1, only four, the two forms of hydrazine, sodium thiosulfate, and perhaps sodium sulfate, appear to be practical additives for enhancing methyl iodide absorption. The others react too slowly to give an appreciable enhancement compared to water alone.

<Appendix 5.4.5> Overall absorption rate

A model formulated from absorption theory for stagnant drops and stagnant wall films is described as follows. The overall washout for reactive sprays is

$$\lambda = \lambda_{drops} + \lambda_{wall\ film} \quad (30)$$

The washout rate constant for spray drops, λ_{drops} , may be written as

$$\lambda_{\text{drops}} = \frac{FHE}{V} \quad (31)$$

where F = spray flow rate, m^3/sec ,

H = methyl iodide partition coefficient,

E = fractional saturation achieved during single pass,

V = volume of containment gas space, m^3 .

The numerical value of E should be obtained from eq. (20) using the relation

$f = 3Q/4\pi d^3 c^*$. In selecting a drop size, it would be best to break the drop size spectrum into increments, and integrate eq. (31) over the spectrum. If a mean drop size is to be used, the volume median diameter is recommended because it will provide a conservative estimate of washout rate. (21)

The washout rate constant for wall films, $\lambda_{\text{wall film}}$ may be expressed as

$$\lambda_{\text{wall film}} = \frac{qA}{VC_g}$$

where $q/C_g = \frac{\text{absorption rate per unit area}}{\text{gas phase concentration}}$, m/sec .

A = surface area of wall film, m^2

V = volume of gas space, m^3

The numerical value of q/C_g can be obtained from eq. (23) with a lower limit to absorption of $\frac{\partial C}{\partial z} = 0$, then eq. (23) becomes

$$0 = D \frac{d^2 C}{dx^2} - KC \quad (33)$$

For this steady state model, the boundary conditions are

$$C = C^* \text{ at } x = 0$$

$$\frac{dC}{dx} = 0 \text{ at } x = \delta$$

The second of these boundary values is based on the assumption that there is no net transfer of methyl iodide to the solid wall. The absorption rate per unit area of interface is

$$q = C^* \sqrt{kD} \tanh \left(\sqrt{\frac{k}{D}} \delta \right) \quad (34)$$

From eq. (34), q/C_g is given by

$$q/C_g = H \sqrt{kD} \tanh \left(\sqrt{k/D} \delta \right) \quad (35)$$

where H = partition coefficient = C^*/C_g
 k = first order reaction rate constant, sec^{-1}
 D = diffusivity of methyl iodide in water, m^2/sec .
 δ = thickness of wall film, m.

Film thickness, δ , may be predicted from laminar flow theory, which relates the film thickness to the film flow rate and fluid viscosity. The film thickness is given by (21).

$$\delta = \left[\frac{3\nu \dot{V}}{g} \right]^{1/3} \quad (36)$$

where δ = film thickness on vertical wall
 ν = kinematic viscosity of water film,
 $\dot{V}$ = film flow rate per length of perimeter,
 g = acceleration due to gravity

For slowly reacting sprays, a simple model may be used because the reaction rate is too slow to enhance absorptions during a single pass. Solutions which fit this category are pure or buffered boric acid, and basic borax solutions containing sodium hydroxide. Eq. (29) applies for these sprays. The spray lambda implicit defined by eq. (29) is

$$\lambda = \frac{F \cdot H}{V_g (F/V_g + k)} \quad (37)$$

F = total spray rate, m^3/sec ,

V_g = volume of gas space, m^3

V_L = volume of spray liquid in containment vessel sump, m^3 ,

k = first order reaction rate constant, sec^{-1}

Appendix 5.5 Models for Aerosol particle washout

Appendix 5.5.1 Washout by spray drops

The removal of aerosol particles by sprays is a more complex process than gaseous absorption because a number of mechanisms contribute significantly to capture. In a review of particle washout by containment sprays, Ritzman et. al. ⁽¹⁶⁾ list the following mechanisms.

- . Brownian diffusion
- . Diffusiophoresis
- . Interception
- . Inertial impaction

The relative importance of each of the contribution mechanisms depends on spray and aerosol properties. Ritzman, et. al. ⁽¹⁶⁾ have evaluated each of these mechanisms for typical LWR containment conditions for a 1,210 micron diameter spray drop.

The dominant mechanisms for capture of particles in the 0.1 micron to 1.5 micron range are diffusionophoresis and interception. For particles with diameters larger than about two microns, inertial impaction becomes the dominant mechanism. Brownian diffusion controls capture of particles smaller than 0.1 micron diameter.

The spray removal rate for aerosols can be related to spray parameters and to the single drop collection efficiency by considering the spray to be an assembly of single drops. The relevant equation is (22):

$$-\frac{dc}{c} = \lambda_s dt = \frac{3hFE}{2dV} dt \quad (38)$$

where C = aerosol concentration, kg/m^3

λ = spray removal rate constant, sec^{-1} .

h = drop fall height, m ,

F = spray flow rate, m^3/sec ,

E = single drop collection efficiency,

d = mean spray drop diameter, m

V = volume of contained gas phase, m^3

The quantities h , F , and V are parameters of the containment system design. The drop diameter, d , is determined by the spray nozzle design and operating conditions, and is subject to direct experiment measurement. The most difficult-to-determine parameter in eq. (38) is the single drop collection efficiency, E .

From a review of available large scale test results on aerosol washout, a conservative estimate of particulate iodine washout can be obtained by choosing $(E/d) = 0.1 \text{ Cm}^{-1}$ for C/C_0 values from 1 to 0.01, and 0.01 Cm^{-1} for C/C_0 value smaller than 0.01⁽²²⁾. Mathematically this can be stated in terms of washout λ 's

$$\lambda = \frac{3hF}{2V} (0.1 \text{ Cm}^{-1}) \quad \text{for } 0.01 \leq C/C_0 \leq 1.0 \quad (39)$$

$$\lambda = \frac{3hF}{2V} (0.01 \text{ Cm}^{-1}) \quad \text{for } C/C_0 \leq 0.01 \quad (40)$$

<Appendix 5.5.2> Deposition of particles on surfaces

Airborne particles are known to be removed by a number of mechanisms even if sprays do not operate.⁽²³⁾ Dominant among these is gravity settling⁽²³⁾ onto horizontal surfaces. Wall plating occurs by diffusion, by thermophoresis, by diffusiophoresis, and by turbulence in the wall boundary layer. All of these mechanisms lead to removal rates which are small compared to the spray removal rate, and therefore surface deposition

of particles had usually been neglected in site evaluations.

<Appendix 5.6> Summary and Conclusions

Spray systems may be included in containment vessels as an engineered safety feature to reduce pressure and scrub airborne containments in the unlikely event of a loss-of-coolant accident. The effectiveness of spray scrubbing is important in the siting of power reactors, because the spray removal rate directly affects the calculated radiation dose from the design basis LOCA which would be received by people in the plant's vicinity.

Specific conclusions and summary statements are listed as follows:

- 1) Mathematical models are available to conservatively predict washout for the several physicochemical forms of iodine.
- 2) For elemental iodine, the most abundant airborne iodine species, a stagnant film model has been adopted by the NRC staff. This model is a simplified form of the equation for absorption by a rigid sphere accounting for a mass transfer resistance in both the gas and liquid phase.
- 3) Absorption of methyl iodide, the most persistent iodine form expected to be present in postaccident atmospheres, is predicted by a model in which it is assumed that both falling drops and wall films are stagnant. For most spray solutions methyl iodide absorption is too slow to appreciably affect two hour dose calculations.
- 4) Aerosol particle washout is predicted using a simple model in which conservative estimates of the single drop collection efficiency are obtained from large scale experiments.

- 5) The composition of spray solutions plays an important role in the absorption rate of elemental iodine because the numerical value of the partition coefficient applicable to the spray washout depends on solution reactions. Experimentally derived partition coefficients are available for plain-boric acid, for sodium hydroxide solutions, for trace level hydrazine solutions, and for solutions of sodium thiosulfate.
- 6) Methyl iodide is absorbed only slowly by spray solutions which do not contain special additives. Of the spray solutions currently in use, only sodium thiosulfate at 1 wt. percent enhances the absorption rate to an appreciable extent. For thiosulfate solution, absorption by wall liquid films is typically more important than that by falling spray drops.
- 7) Surface deposition of elemental iodine can be predicted using a model in which it is assumed that the limiting transfer resistance resides in the gas phase boundary layer. Predicted surface deposition rates are small compared to spray washout rates.

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Table A5.1 Summary of Reaction Rates of Methyl Iodide
in Aqueous Solutions⁽²⁰⁾

Reactant	k**	Activation Energy (kCal/mole)	Temperature Range Studied(°C)	k Extrapolated at 120°C
H ₂ O	1.4x10 ^{-9*}	>24.8	30-93	0.00006
F ⁻	7.08x10 ⁻⁸	25.2	-	0.002
Cl ⁻	3.3x10 ⁻⁶	21.97	-	0.025
Br ⁻	4.16x10 ⁻⁵	19.31	-	0.11
OH ⁻	6.36x10 ⁻⁵	22.22	30-70	0.55
SCN ⁻	3.58x10 ⁻⁴	19.95	25-35	1.2
I ⁻	4.71x10 ⁻⁴	17.58	-	1.6
CN ⁻	5.76x10 ⁻⁴	20.47	20-55	2.5
N ₂ H ₄	1.1x10 ⁻³	20.2	25-65	10
A _g ⁺	2.61x10 ⁻³	19.4	15-45	7
1,1 dimethyl hydrazine	7.7x10 ⁻³	18.4	25-35	13
S ₂ O ₃ ²⁻	3x10 ⁻²	18.88	10-25	70
SO ₃ ²⁻	3.5x10 ⁻²	-	25	-

* Calculated from first order rate constant using H₂O
concentration = 55 moles/liter

** Bimolecular reaction rate constant at 25°C (liter mole⁻¹ sec⁻¹)

Appendix 6. Atmospheric Dispersion

Appendix 6.1 The General Atmospheric Transport Model

An atmospheric transport model is an important link which connects an accidental or routine release to the resulting radiological consequences. For a fixed release quantity, the magnitude of the dose and duration of exposure to deliver a given dose, depend upon the characteristics of the transport medium--the atmosphere--during the time of release.

An appropriate starting point for describing the turbulent diffusion radioactive pollutant released to the atmosphere is a generalized three-dimensional, time dependent diffusion equation including sources,

$$\frac{\partial X(\vec{r},t)}{\partial t} = \nabla \cdot \vec{J}(\vec{r},t) + S(\vec{r},t) \quad (1)$$

where

$X(\vec{r},t)$ = concentration of the given material at position $\vec{r}$, and time, t , in curies/ m^3 .

$\vec{J}(\vec{r},t)$ = diffusive mass flux of the airborne material in curies/ m^2/s , and

$S(\vec{r},t)$ = source function in curie/ m^3/s .

Under the assumption that diffusion can be expressed by Fick's law⁽¹⁾, the mass flux vector can be written in Cartesian coordinates as:

$$\vec{J}(\vec{r},t) = \hat{i}K_x \frac{\partial X}{\partial x} + \hat{j}K_y \frac{\partial X}{\partial y} + \hat{k}K_z \frac{\partial X}{\partial z} \quad (2)$$

where the K 's are space and time dependent diffusion coefficients^(1,2).

If the source is an instantaneous point source at $\vec{r} = (x_0, y_0, z_0)$ and $t = 0$, the source term becomes

$$S(x,y,z,t) = Q\delta(x-x_0)\delta(y-y_0)\delta(z-z_0)\delta(t) \quad (3)$$

where

Q = magnitude of the instantaneous release in curies,

δ = Dirac delta function.

With the source thus defined, and the further restriction that the K_i 's are each space and time independent it is possible to solve Equation (1) by means of Laplace Transforms and obtain the solution presented by Roberts⁽³⁾.

$$X(x,y,z,t) = \frac{Q}{(4\pi t)^{3/2} \sqrt{K_x K_y K_z}} \exp\left[-\frac{(x-x_0)^2}{4K_x t} - \frac{(y-y_0)^2}{4K_y t} - \frac{(z-z_0)^2}{4K_z t}\right] \quad (4)$$

This is the fundamental equation of diffusion in the atmosphere and is completely valid for an infinite static air mass or an infinite air mass in which the coordinate system follows the trajectory of the wind vector. The concept of applying Equation (4) to atmospheric problems by developing expressions for the K 's is referred to as "K Theory". This theory has only been applied successfully to diffusion problems in the free atmosphere and has provided useful theoretical results, as described by Gifford⁽¹⁾.

Most workers in the area of airborne dispersion make use of statistical theory put forth by Taylor⁽⁴⁾. In statistical theory, the form of Equation (4) as a solution to the diffusion equation is retained. However, it is postulated that a quantity of material instantaneously released to the free static atmosphere assumes a Gaussian distribution along the x , y and z axes.

Under this assumption the basic dispersion equation for an instantaneous point source is

$$X(x, y, z, t) = \frac{Q}{(2\pi)^{3/2} \sigma_x \sigma_y \sigma_z} \exp\left[-\frac{(x-x_0)^2}{2\sigma_x^2} - \frac{(y-y_0)^2}{2\sigma_y^2} - \frac{(z-z_0)^2}{2\sigma_z^2}\right] \quad (5)$$

The σ 's are the mean square deviations of the spreading plume which are time dependent parameters. By comparing Equation (5) to Equation (4), it is observed that they are exactly the same if one makes the substitution $K_1 = \sigma_x^2/2t$. However, Equation (5) cannot be obtained from Equation (1) by making this substitution. An operating coordinate system can be chosen so that the origin is at ground level at or beneath the point of emission, with x-axis extending horizontally in the direction of the mean wind velocity $\bar{u}$. The y-axis is in the horizontal plane perpendicular to the x-axis, and the z-axis extends vertically. H is the height of the plume centerline when it becomes essentially level, and is the sum of the physical stack height, h , and, the plume rise, ΔH .

When the expanding cloud reaches the ground surface, some or all of the suspended material will be reflected. The resulting higher concentration at ground level can be accounted for by assuming an image source located at $z = -H$. With these modifications Equation (5) becomes:

$$X(x, y, z, t) = \frac{Q}{(2\pi)^{3/2} \sigma_x \sigma_y \sigma_z} \exp\left[-\frac{(x-\bar{u}t)^2}{2\sigma_x^2} - \frac{y^2}{2\sigma_y^2}\right] \times \left\{ \exp\left[-\frac{(z-H)^2}{2\sigma_z^2}\right] + R_f \exp\left[-\frac{(z+H)^2}{2\sigma_z^2}\right] \right\} \quad (6)$$

R_f is the fraction of the total cloud concentration at ground level reflected back into the atmosphere.

Appendix 6.2 A Continuous Uniform Release

A continuous plume may be considered as made up of an infinite number of instantaneous or puff releases sequentially arranged with a vanishingly small time interval between puffs. The quantity of material released is expressed as a release rate $\dot{Q}$ (curies/sec). The instantaneous increase in ground level concentration of a pollutant, $d_x(x, y, 0, t)$, at any downwind position can be viewed as resulting from an instantaneous source of magnitude $Q = \dot{Q}dt$. This allows one to re-write Equation (6) as

$$d_x(x, y, 0, t) = \frac{\dot{Q}dt}{(2\pi)^{3/2} \sigma_x \sigma_y \sigma_z} [1+R_f] \exp\left\{-\left[\frac{(x - \bar{u}t)^2}{2\sigma_x^2} + \frac{y^2}{2\sigma_y^2} + \frac{H^2}{2\sigma_z^2}\right]\right\} \quad (7)$$

The average steady state ground level concentration is obtained by integrating the Equation (7) from $t = 0$ to $t = \infty$. This could be a formidable task because of the time dependence of the σ 's. However, it has been pointed out^(1,5) that diffusion along the x-axis can be ignored relative to wind transport. This allows x to be treated as constant and σ_z and σ_y to become function of x , since $t = x/\bar{u}$. Removing this troublesome time dependence allows Equation (7) to be integrated giving:

$$X(x, y, 0) = \frac{\dot{Q}(1+R_f)}{2\pi\sigma_y\sigma_z\bar{u}} \exp\left[-\frac{y^2}{2\sigma_y^2} - \frac{H^2}{2\sigma_z^2}\right] \quad (8)$$

The spreading plume is constantly depleted at ground level by such processes as dry deposition and rainout. Because of these pro-

cesses, a ground level observer at a downwind location x sees a modified source $Q(x)$. In order to account for these effects R_f is replaced by

$$R_f = 2 \frac{\dot{Q}(x)}{\dot{Q}} - 1.$$

Then Equation (8) for a continuous uniform release becomes

$$X(x, y, 0) = \frac{\dot{Q}(x)}{\pi \sigma_y \sigma_z \bar{u}} \exp\left[-\frac{y^2}{2\sigma_y^2} - \frac{H^2}{2\sigma_z^2}\right] \quad (9)$$

Briggs⁽¹⁴⁾ has developed a set of empirical correlations for σ_y and σ_z as functions of downwind position which corresponds to Pasquill's categories on open terrain. These formulations are listed in Table A6.1.

Appendix 6.3 Instantaneous Release

The instantaneous or "puff" release is an approximation to the type of emission associated with a very rapid venting of material to the atmosphere. The puff, once formed, moves away from the source with a speed and direction determined by the wind at the moment of release. The instantaneous ground level concentration due to an instantaneous source can be obtained by the time-integral from Equation (6) in the same way as Equation (9).

$$X(x, y, 0) = \frac{Q(x)}{\pi \sigma_y \sigma_z \bar{u}} \exp\left[-\frac{y^2}{2\sigma_y^2} - \frac{H^2}{2\sigma_z^2}\right] \quad (10)$$

where $Q(x)$ is the source modified for deposition and $R_f = 2Q(x)/Q - 1$.

Instantaneous release processes are frequently associated with an explosion or a short and rapid burning. For this reason, the puff will have an initial volume of dilution V_0 , in the atmosphere. Taking

this into consideration, the expression for the ground level exposure from the instantaneous source is:

$$X(x,y,0) = \frac{Q}{\pi(\sigma_y \sigma_z \bar{u} + V_0)} \exp\left[-\frac{y^2}{2\sigma_y^2} - \frac{H^2}{2\sigma_z^2}\right] \quad (11)$$

This initial dilution increases the effective exposure time by an amount $D_0/\bar{u}$ where D_0 is the initial ground level diameter of the puff.

Slade⁽¹⁴⁾ has pointed out that dispersion coefficients correlated to continuous emissions are not acceptable for puff releases. He has shown that the Gaussian model is acceptable, as long as the empirical correlations for σ_y and σ_z are modified. Slade's formulation is given in Table A6.2. Brigg's correlations, which allow for more plume spread due to longer sampling time, are used only to estimate average sector doses from continuous releases while Slade's correlations are used to determine the maximum exposure from an instantaneous release along the plume centerline.

If the concentration is to be calculated along the centerline of the plume ($y = 0$), the Equation (10) can be simplified as follows:

$$X(x,0,0) = \frac{Q}{\pi\sigma_y \sigma_z \bar{u}} \quad (12)$$

Appendix 6.4 Effects of Stability

The value of σ_y and σ_z vary with the turbulent structure of the atmosphere, height above the surface, surface roughness, sampling time over which the concentration is to be estimated, wind speed, and distance from the source. For the parameter values given here, the sampling time is assumed to be about 10 minutes, the height to be the lowest several hundred meters of the atmosphere, and surface to be relatively open country. The turbulent structure of atmosphere and

wind speed are considered in the stability classes, and the effect of distance from the source is embodied in the graphs determining the parameter values. Mean square deviations, σ_y and σ_z , used in the dispersion equations are estimated from the stability of atmosphere, which is in turn estimated from the wind speed at height of about 10 meters and, during the day, the incoming solar radiation or, during the night, the cloud cover⁽⁶⁾. Pasquill⁽⁶⁾ proposed a series of six atmospheric stability categories, which are shown in Table A6.1, each having a different empirical correlation for σ_y and σ_z . Class A is the most unstable, class F the most stable class considered here. Note that the neutral class D can be assumed for overcast conditions during day or night, regardless of wind speed.

"Strong" incoming solar radiation corresponds to a solar altitude greater than 60° with clear skies; "slight" insolation corresponds to a solar altitude from 15° to 35°. Table 170, Solar Altitude and Azimuth, in Smithsonian Meteorological Tables (List, 1951) can be used in determining the solar altitude. Cloudiness will decrease incoming solar radiation and should be considered together with solar altitude in determining solar radiation. Incoming radiation that would be strong with clear skies can be expected to be reduced to moderate with broken (5/8 to 7/8 cloud cover) and to slight with broken low clouds. An objective system of classifying stability from hourly meteorological observations based on the above method has been suggested⁽⁷⁾.

Appendix 6.5 Plume Depletion

In order to accurately model the diffusion of radioactive material in the atmosphere, the rate at which contaminants are deposited from

air to ground must be known. Processes by which materials are removed from the air are classified as either wet or dry deposition.

Dry deposition occurs by gravitational settling, molecular exchange, surface impaction, electrostatic attraction and chemical interaction at ground level. Gravitational fall is insignificant for gaseous species and is neglected in the present evaluation. Wet deposition is the removal of species from atmosphere to ground by rainfall.

In general, wet and dry deposition cannot be specified precisely. However, it is possible to determine the limiting effects of these processes on consequences of radioactive release. Lower limits are obtained by ignoring deposition completely. Upper limits are dictated by the limited capacity of the atmosphere to carry material to ground level. Maximum deposition quantities have been estimated by experiment⁽⁸⁾.

Dry deposition rates are proportional to the immediate ground level concentration. Chamberlin⁽⁹⁾ has defined the dry deposition rate w (curies/m²/s) as the product of the average ground level concentration $\bar{x}(x,y,0)$ and the deposition velocity v_d as:

$$w(z,y) = v_d \bar{x}(x,y,0) \quad (14)$$

If the depletion of the plume is to be allowed for, $\bar{Q}$ or Q must be replaced by an effective source strength $\bar{Q}(x)$ or $Q(x)$ which is a function of x .

Application of a mass balance to a control volume surrounding the plume shows that $Q(x)$ is just:

$$\frac{\partial Q(x)}{\partial x} = - \int_{-\infty}^{+\infty} w(x,y) dy \quad (15)$$

Substituting Equations (10) and (14) into Equation (15) gives

$$\frac{\partial Q(x)}{\partial x} = \sqrt{\frac{2}{\pi}} \frac{Q(x)}{\sigma_z(x)} \frac{v_d}{u} \exp\left[-\frac{H^2}{2\sigma_z^2}\right] \quad (16)$$

Rearranging and integrating Equation (16) with respect to x yields

$$Q(x) = Q \exp\left[-\sqrt{\frac{2}{\pi}} \frac{v_d}{u} I(x)\right] \quad (17)$$

where

$$I(x) = \int_0^x \frac{dx}{\sigma_z(x) \exp[H^2/2\sigma_z^2(x)]} \quad (18)$$

Equations (10), (14), (17), (18) are used to determine surface level concentrations and dry deposition rates when the deposition velocities are given.

Wet deposition can be described by a simple removal rate, r , per unit air volume, as:

$$r = A \bar{X}(x,y,z) \quad (19)$$

where

A = washout coefficient (sec^{-1})

$\bar{X}$ = concentration in washout area.

For precipitation that is just beginning at a downwind position x_0 , the deposition rate at that position is:

$$w_0 = A \int_0^A \bar{X}(x_0, y, z) dz \quad (20)$$

The upper limit for this integral is the altitude from which the rain

fails. If it is assumed that the plume is contained below this level, then

$$w_0 = \frac{AQ}{\bar{u}\sigma_y} \sqrt{\frac{2}{\pi}} \exp\left[-\frac{y^2}{2\sigma_y^2}\right] \quad (21)$$

The Equation (21) assumed that the plume profile does not change with elevation because of wind shear. The concentration can be modified for wet removal up to some distance x , greater than x_0 , by an attenuation term. With this modification the wet deposition rate at x, y from a cloud which originates at x_0 is

$$w = \frac{AQ\sqrt{2}}{\bar{u}\sigma_y\sqrt{\pi}} \exp\left[-\frac{y^2}{2\sigma_y^2}\right] \exp\left[-\frac{\Lambda(x-x_0)}{\bar{u}}\right] \quad (22)$$

Typical washout coefficients are given in Table A6.2.

Appendix 6.6 Effective Height of Emission

In most problems one must estimate the effective stack height, H , at which the plume becomes essentially level. If the plume is caught in the turbulent wake of the stack or of the buildings in the vicinity of the stack, the effluent will be carried rapidly downward toward the ground (aerodynamic downwash). If the plume is emitted free of these turbulent zones, a number of emission factors and meteorological factors influence the rise of the plume. The emission factors are: velocity of the effluent at the top of the stack, v_s ; temperature of the effluent at the top of the stack, T_s ; and diameter of the stack opening, d . The meteorological factors influencing plume rise are wind speed, $\bar{u}$; temperature of the air, T_a ; shear of the wind with height, $d\bar{u}/dz$; and atmospheric stability. No theory on plume rise

takes into account all of these variables; even if such a theory could be devised, measurements of all of the parameters would seldom be available. Most of the equations that have been formulated for evaluating the effect of height of emission are semi-empirical.

The Holland's⁽¹⁰⁾ equation was developed with experimental data from larger sources than those of Moses and Strom⁽¹¹⁾ (stack diameters from 1.4 to 4.3 meters and stack temperatures from 82°C to 204°C); this equation frequently underestimates the effective height of emission; therefore its use often provides a slight safety factor.

Holland's equation is:

$$\Delta H = \frac{v_s d}{\bar{u}} (1.5 + 2.68 \times 10^{-3} P \frac{T_s - T_a}{T_s} d) \quad (23)$$

where

ΔH = the rise of the plume above the stack, m

v_s = stack gas exit velocity, ms^{-1}

d = the inside stack diameter, m

$\bar{u}$ = wind speed, ms^{-1}

P = atmospheric pressure, mb

T_s = stack gas temperature, K

T_a = air temperature, K

Holland suggests that a value between 1.1 and 1.2 times the ΔH from the equation should be used for unstable conditions; a value between 0.8 and 0.9 times the ΔH from the equation should be used for stable conditions.

Since the plume rise from a stack occurs over some distance downwind, equation (23) should not be applied within the first few hundred

meters of the stack.

If the effective heights of emission were the same under all atmospheric conditions, the highest ground-level concentrations from a given source would occur with the lightest winds. Generally, however, emission conditions are such that the effective stack height is an inverse function of wind speed as indicated in Equation (23). The maximum ground-level concentration occurs at some intermediate wind speed, at which a balance is reached between the dilution due to wind speed and the effect of height emission. The critical wind speed will vary with stability. In order to determine the critical wind speed, the effective stack height as a function of wind speed should first be determined. The maximum concentration for each wind speed and stability can be obtained as a function of effective height of emission and stability.

The influence of mechanical turbulence around a building or stack can significantly alter the effective stack height. This is especially true with high winds, when the beneficial effect of high stack gas velocity is at minimum and the plume is emitted nearly horizontally. The region of disturbed flow surrounds an isolated building, generally to at least twice its height and extends downwind 5 to 10 times its height. Building the stack 2.5 times the height of the highest building adjacent to the stack usually overcomes the effects of building turbulence⁽¹²⁾. Ensuring that the existing velocity of the stack gas is more than 1.5 times the wind speed will usually prevent downwash in the wake of the stack.

The Regulatory Guides 1.3 and 1.4 suggest use of the building wake correction model presented by Slade⁽¹³⁾. This model is to be applied only to ground level releases of eight hours or less duration. The dispersion correction factor is limited to the range of 1/3 to 1. The classic way of handling the turbulence-enhancement effect in the centerline diffusion equation in Regulatory Guides 1.3 and 1.4 has been to add to the plume size parameters a term that accounts for the enhanced mixing in the lee of the building:

$$\lambda/Q = \frac{1}{\pi \Sigma_y \Sigma_z \bar{u}} \quad (24)$$

where

$$\Sigma_y = (\sigma_y^2 + CA/\pi)^{1/2}$$

$$\Sigma_z = (\sigma_z^2 + CA/\pi)^{1/2}$$

A = the minimum cross sectional area of the reactor building (m²)

C = the fraction of the area over which the plume is dispersed
(~ 0.5 in Reg. Guides 1.3 and 1.4).

The exact numerical value for C will have to be determined by suitable experiments. Gifford⁽¹⁵⁾ suggested that, as a reasonable estimate, $1/2 \leq C \leq 2$. The reason for choosing these particular bounds, which were actually no more than a guess, was to provide, in absence of suitable experimental data, usable numbers for concentration estimation. According to Barry⁽¹⁶⁾, who made an interesting and useful summary of the results of a number of experiments, studies with wind-tunnel models have suggested values of C near the lower of these limits, namely, C = 0.5 to 0.67. Thus, NRC has chosen conservatively lower bounds of C. Of course, it is not impossible that larger values of C may be found

if suitable full-scale atmospheric experiments are performed, particularly in unstable light-wind conditions. A further potential source of conservatism in the dispersion model is the absence of any consideration of the relief offered by "radioactive buoyancy" of a plume containing a high concentration of radioactive material. If the concentration is sufficiently high, the temperature of the air in the plume becomes high enough to make the plume buoyant with respect to the surrounding air. Thus, even for a ground-level release, the axis of the plume is no longer at the surface, so that the concentration there is reduced, thereby decreasing doses due to inhalation, and there is some "shielding-by-distance" from the gamma radiation responsible for the whole body dose due to external radiation. A good summary of the effects of this phenomenon was provided by Beattie in Ref. 17. His estimates have been confirmed to a large extent by later work in other establishments.

However, in the case of accidents of the type visualized in Regulatory Guides 1.3 and 1.4, the rate of release of active material would be so slow, if the requirements of the guides were met, that the radioactive buoyancy would be negligible, i.e., if the objectives of the Regulatory Guides are met, these would, in practice, be no factor of conservatism on account of radioactive buoyancy.

References: Appendix 6

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Table A6.1 Briggs' Formulas for $\sigma_y(x)$ and $\sigma_z(x)$ for Open Terrain

Pasquill Condition	$\sigma_y(x)$, m	$\sigma_z(x)$, m
A	$0.32 \times (1+0.0001x)^{-1/2}$	$0.26x$
B	$0.16 \times (1+0.0001x)^{-1/2}$	$0.12x$
C	$0.11 \times (1+0.0001x)^{-1/2}$	$0.08 \times (1+0.0002x)^{-1/2}$
D	$0.08 \times (1+0.0001x)^{-1/2}$	$0.06 \times (1+0.0015x)^{-1/2}$
E	$0.06 \times (1+0.0001x)^{-1/2}$	$0.03 \times (1+0.0003x)^{-1}$
F	$0.04 \times (1+0.0001x)^{-1/2}$	$0.016 \times (1+0.0003x)^{-1}$

Table A6.2 Slade's Formulas for $\sigma_y(x)$ and $\sigma_z(x)$ in Puff Releases

Condition	$\sigma_y(x)$, m	$\sigma_z(x)$, m
Unstable	$0.14(x)^{0.92}$	$0.53(x)^{0.75}$
Neutral	$0.06(x)^{0.92}$	$0.15(x)^{0.70}$
Very Stable	$0.02(x)^{0.89}$	$0.05(x)^{0.61}$

Table A6.3 Relation of Turbulence Types to
Weather Conditions⁽¹³⁾

A - Extremely unstable conditions D - Neutral conditions*
 B - Moderately unstable conditions E - Slightly stable conditions
 C - Slightly unstable conditions F - Moderately stable conditions

Surface wind speed, m/sec	Daytime insolation			Nighttime conditions	
	Strong	Moderate	Slight	Thin overcast or $\geq 4/8$ cloudiness†	$\leq 3/8$ cloudiness
<2	A	A-B	B		
2	A-B	B	C	E	F
4	B	B-C	C	D	E
6	C	C-D	D	D	D
>6	C	D	D	D	D

* Applicable to heavy overcast, day or night.

†The degree of cloudiness is defined as that fraction of the sky above the local apparent horizon which is covered by clouds.

Table A6.1. Bounds and Approximations for Wet and Dry Removal of Particles and Gases ⁽⁸⁾

	Minimum Expected ^(a)	Average Expected ^(b)	Maximum Expected
Wet removal ^(c) rate (sec^{-1})	10^{-5}	Stable ^(d) : 10^{-4} , Not stable ^(e) : 10^{-3}	10^{-2}
Dry deposition velocity (m/sec)	10^{-3}	10^{-2}	10^{-1}

^(a) Except for gases barely soluble in water (e.g., the noble gases); then use zero.

^(b) Based on field experimental data for micron-size particles and soluble gases.

^(c) Multiply tabulated numerical values by $(p/1 \text{ mm hr}^{-1})$, where p is the precipitation rate in rainwater equivalent.

^(d) Warm frontal storm.

^(e) Convective storm.

Appendix 7. Review of estimated doses for Class 8 accidents

Various applicants for licenses have endeavored to demonstrate the degree of conservatism in the Reg. Guide 1.4 assumptions by calculating the whole body and thyroid doses at the EAB and CPZ using assumptions of their own, which they have described as "conservative", "realistic" and "Environmental Report Model".

In order to compare these estimates on a common basis it is convenient to derive a "Relaxation Factor" for each case, where this factor is defined as follows:

$$\text{Relaxation Factor} = \frac{\text{Retention factor for Reg. Guide 1.4 analysis}}{\text{Retention factor for Applicant's analysis in}}$$

"Retention Factor" in this expression is defined for each analysis model as

$$\text{Retention Factor} = \frac{\text{Amount of Radioactive material released to containment}}{\text{Amount of Radioactive material released to atmosphere.}}$$

The Retention Factor has been calculated by working back from the Applicant's estimated EAB and LPZ doses, using his stated atmospheric dilution factors, to obtain the denominator of the expression; the numerator is simply the Applicant's source term.

Table A7.1 Review of Estimated Doses for Class 8 Accidents

Relaxation Factors for Containment Design

(a) (Virgil C. Summer plant, Westinghouse)

Accidents Dose	Location	Rod Ejection Accident		
		Conservative Analysis	Realistic Analysis	Environmental Report Model
Whole body (Xe^{133}) gamma dose	EAB	6.514×10^3	5.434×10^6	4.0716×10^5
	LPZ	3.517×10^4	2.8093×10^7	1.9206×10^6
Thyroid inhalation (I^{134})	EAP	3.398	1.154×10^5	4.158×10^2
	LPZ	7.4075	4.054×10^5	9.0627×10^2
Loss of Coolant Accident				
Whole body gamma dose	EAB	2.77×10^5	2.359×10^7	1.511×10^1
	LPZ	1.4814×10^4	6.667×10^6	8.08026×10^4
Thyroid inhalation	EAP	94.34	1.275×10^7	1.012×10^1
	LPZ	4.949×10^2	1.765×10^7	6.46×10^4
- Dist: to EAB (meters): 1.609 (m) - source terms - - Distance to LPZ (meters): 4.827 (m)				
Rod Ejection Accident				
		Conservative Analysis	Realistic Analysis	Environmental Report Model
Whole body gamma dose (Xe^{133}), (Ci)		1.71×10^6	9.89×10^2	3.42×10^5
Thyroid inhalation (I^{134}), (Ci)		7.16×10^5	5.31×10^1	1.17×10^5
LOCA				
		Conservative Analysis	Realistic Analysis	Environmental Report Model
Whole body gamma dose (Xe^{133}), (Ci)		1.44×10^6	5.9×10^4	3.3×10^5
Thyroid inhalation (I^{132}), (Ci)		2.84×10^5	2.57×10^2	7.16×10^1

Relaxation Factors for Containment Design (con'd)

(b) (Midland Power Plant, B&W)

Accidents Dose	Rod Ejection Accident			
	Location	Conservative Analysis	Realistic Analysis	Environmental Report Model
Whole body (Xe^{133}) gamma dose	EAB	1.4386×10^2	6.427×10^{-1}	1.028×10^4
	LPZ	2.924×10^2	4.1186×10^{-1}	2.0862×10^4
Thyroid (I^{131}) inhalation	EAB	4.6	6.578×10^{-2}	5.359×10^{-2}
	LPZ	3.866	3.636×10^{-2}	4.597×10^{-2}
Loss of Coolant Accident				
	Location	Conservative Analysis	Realistic Analysis	Environmental Report Model
Whole body (Xe^{133}) gamma dose	EAB	15.15	3.41×10^{-4}	9.4692×10^{-2}
	LPZ	38.456	2.4023×10^{-4}	2.4035×10^{-3}
Thyroid (I^{131}) inhalation	EAB	1.6	1.519×10^{-3}	8.13×10^{-2}
	LPZ	4.178	4.1898×10^{-3}	2.0917×10^{-3}
- Distance to EAB (meters): 500 (m) < source terms > - Distance to LPZ (meters): 1,600 (m)				
Rod Ejection Accident				
		Conservative Analysis	Realistic Analysis	Environmental Report Model
Whole body gamma dose (Xe^{133}), (Ci)		7.37×10^6	3.16×10^6	1.29×10^6
Thyroid inhalation (I^{131}), (Ci)		1.07×10^6	1.37×10^5	1.8375×10^5
LOCA				
		Conservative Analysis	Realistic Analysis	Environmental Report Model
Whole body gamma dose (Xe^{133}), (Ci)		1.27×10^8	8.31×10^6	2.54×10^6
Thyroid inhalation (I^{131}), (Ci)		1.84×10^7	3.64×10^3	7.35×10^4

Relaxation Factors for PWR Dry Containment Design

(c) (San Onofre 2d3 Plant, Combustion Eng.)

Dose	Accidents	Rod Ejection Accident			
		Location	Conservative Analysis	Realistic Analysis	Environmental Report Model
Whole body gamma dose (Xe^{135})	EAB		2.006×10^2	1.923×10^4	1.7868×10^4
	LPZ		1.8178×10^3	1.9087×10^4	1.773×10^5
Thyroid inhalation (I^{131})	EAB		6.146	9.29×10^2	6.202×10^2
	LPZ		15.64	2.656×10^2	1.578×10^3
Loss of Coolant Accident					
	Location		Conservative Analysis	Realistic Analysis	Environmental Report Model
Whole body gamma dose (Xe^{135})	EAB		11.11	2.382×10^4	6.825×10^2
	LPZ		1.424×10^2	1.462×10^4	8.928×10^3
Thyroid inhalation (I^{131})	EAB		1.714	2.97×10^3	1.676×10^3
	LPZ		13.04	2.4×10^3	6.025×10^3
- Distance to EAB (meters): 576 (m) < source terms >					
- Distance to LPZ (meters): 5.140 (m)					
Rod Ejection Accident					
			Conservative Analysis	Realistic Analysis	Environmental Report Model
Whole body gamma dose (Xe^{135}), (Ci)			5.84×10^6	5.84×10^6	4.925×10^4
			1.15×10^6	1.15×10^6	2.24×10^4
LOCA					
			Conservative Analysis	Realistic Analysis	Environmental Report Model
Whole body gamma dose (Xe^{135}), (Ci)			1.97×10^8	2.53×10^7	3.94×10^6
			4.48×10^7	4.20×10^6	8.96×10^4
Thyroid inhalation (I^{131}), (Ci)					

Appendix 8. The Effect of Internal Diameter and Design Pressure on Containment Cost

Approximate relationships between internal diameter and cost of containment structures for steel lined and for unlined containment are derived in this Appendix.

As discussed in Section 5 of the main text, the major components of cost which are particularly sensitive to the internal diameter are:

- (i) the steel used to resist the pressure load in the shell and dome,
- (ii) the cost of the mild steel liner.

For a thin steel vessel we have the relationships:

$$t = \frac{p}{s} r, \text{ for a cylinder}$$

and

$$t = \frac{p}{s} \frac{r}{2}, \text{ for a spherical vessel}$$

where

t = minimum design thickness of plate

p = design pressure

s = design stress

r = radius of vessel

The surface area, A , of a cylindrical or spherical vessel is proportional to r^2 .

The volume of steel required, V_s , is proportional to tA . Thus we can write $V_s = kr^2rp = kr^3p$ for either cylindrical or spherical vessels. In the case of a containment vessel, p is inversely proportional to the contained volume and thus to $1/r^3$.

Consequently the volume of steel required is, to a first approximation, independent of the radius of the containment vessel. In the case of a reinforced or prestressed vessel, the quantity of steel required to resist the design pressure is also approximately proportional to tA and thus, as in the case of the steel vessel, is virtually independent of the radius.

However, in the case of a reinforced or prestressed concrete vessel the area of the interior surface increases as r^2 but, if a steel liner is used its thickness cannot be diminished as the radius increases. Thus the amount of material required for the liner increases as r^2 and, moreover, the total length of welds is also proportional to r^2 . As shown in the main text, the cost of the liner (less the penetration) is about 30 per cent of the total cost of the containment structure (including the interior concrete work). Thus a 10 per cent increase in radius of a steel-lined containment vessel would add about 6 per cent to the cost of the structure.

Pressure suppression provides a means of reducing design pressure, thus in view of the relationship derived above,

$$V_s = kr^3p$$

a proportional decrease in V_s can be obtained, without decreasing the radius of the structure. In addition, if the structure is a lined concrete one, a saving in liner costs proportional to r^2 would also be obtained by reducing the radius of the structure.

The greater thickness required for a cylindrical vessel, for the same design pressure, makes it worth while to examine the relative

costs of spherical and cylindrical vessels containing the same volume.

Consider a typical cylindrical vessel, with a hemispherical dome end, of radius r_{cy} and height of cylindrical section $a r_{cy}$, and a sphere of radius r_{sp} . The following relationships can be derived:

(i) Volume of containment

$$\text{Cylinder } \frac{\pi r_{cy}^3}{3} (a+2), \quad \text{sphere } \frac{4}{3} \pi r_{sp}^3$$

$$\text{Thus for } V_{cy} = V_{sh}, \text{ we require } r_{cy}/r_{sp} = \left(\frac{4}{3a+2} \right)^{1/3}$$

(ii) Surface area of containment structure

$$\text{Cylinder (excluding base)} \quad 2\pi r_{cy}^2 (a+1), \quad \text{sphere } 4\pi r_{sp}^2$$

$$\text{Thus } A_{cy}/A_{sp} = \frac{(a+1)}{2} \left(\frac{4}{3a+2} \right)^{2/3}$$

(iii) Comparison of volume of steel for pressure retention purposes

$$\text{Cylinder } 2\pi r_{cy}^2 t (2a+1), \quad \text{sphere } 4\pi r_{sp}^2 t$$

$$\text{Thus } V_{s(cy)}/V_{s(sp)} = \left(\frac{2a+1}{2} \right) \left(\frac{4}{3a+2} \right)^{2/3}$$

The values of A_{cy}/A_{sp} and of $V_{s(cy)}/V_{s(sp)}$ as functions of a are shown in Table A8.1.

Table A8.1 Variation in ratios of surface area and of quantities of pressure retaining steel for cylindrical and spherical vessels of the same volume, as function of "a".

a	r_{cy}/r_{sp}	Ratio of surface area	Ratio of quantities of steel
1	0.928	0.862	1.29
2	0.794	0.945	1.575
3	0.714	1.02	1.783
4	0.659	1.08	1.952

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