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**Proceedings of the OECD/NEA/CSNI Workshop on the
Implementation of Hydrogen Mitigation Techniques
Winnipeg, Manitoba, 1996 May 13-15**

**Compte rendu de l'atelier de l'OCDE/AEN/CSIN sur la Mise
en oeuvre des techniques de mitigation de l'hydrogène
Winnipeg (Manitoba), du 13 au 15 mai 1996**





**PROCEEDINGS OF THE OECD/NEA/CSNI WORKSHOP ON THE
IMPLEMENTATION OF HYDROGEN MITIGATION TECHNIQUES**

**Grant W. Koroll (AECL), Chairman
Jürgen Rohde (GRS), Co-Chairman
Jacques Royen (OECD NEA), CSNI Secretariat**

**Winnipeg, Manitoba, Canada
1996 May 13-15**

**Organized by CSNI's Principal Working Group on the Confinement of Accidental
Radioactive Releases (PWG4) - Task Group on Containment Aspects of Severe Accident
Management (CAM), in collaboration with the Whiteshell Laboratories of AECL,
Ontario Hydro and the CANDU Owners Group**

**Whiteshell Laboratories
Pinawa, Manitoba, Canada R0E 1L0
1997**

FOREWORD

The accident at Three Mile Island (TMI) in 1979 focused world attention on the potential threat of unmitigated hydrogen accumulation in containment following a severe reactor accident. In the intervening years, there has been a significant scientific and engineering effort to quantify and improve margins of safety for hydrogen in the aftermath of a severe reactor accident. Hydrogen has been the focus of several industry meetings of experts and state-of-art reviews in the continuing effort to refine and consolidate understanding of hydrogen behaviour.

The Winnipeg Workshop marked a departure from past meetings on hydrogen in reactor safety in that it shifted the focus away from analysis of phenomena related to mixing and combustion and on to application of knowledge in practical measures for mitigation and management of the hydrogen threat.

It was timely that the Workshop was held in Canada. The Canadian program on hydrogen in reactor safety actually pre-dates TMI. The familiar yellow vessels of the Containment Test Facility (CTF) at Whiteshell Labs were already under construction at the time of TMI. Continued Canadian initiative on hydrogen was shown in the year leading up to the Workshop by the commissioning of two major new facilities for hydrogen mixing and combustion and the unveiling of state-of-art catalytic recombiner technology.

The Workshop attracted very high-quality papers on the important activities on hydrogen mitigation from across the world. The authors and the Program Committee deserve commendation for this. Furthermore, the Workshop was well balanced in industry representation, with delegates from nuclear utilities, vendor companies, regulators and research organizations in about equal numbers. This remarkable response was an indication of the timeliness of the topic and of the validity of OECD/NEA/CSNI as a sponsor.

ACKNOWLEDGMENTS

The hosting of the workshop was shared by AECL, Ontario Hydro and the CANDU Owners Group. The financial contribution from these organizations is gratefully acknowledged. The Containment Working Party of COG acted as the local steering committee for the project. Working Party members are Richard Fluke, Keith Weaver, Kimberly Yim, Mike Cormier, Michel Garceau, Sajid Quraishi and Bill Kupferschmidt. Local organization was carried out by the Combustion Section of the AECL Containment Analysis Branch, notably, Calvin Chan, Krishna Kumar, David Whitehouse, William Dewit and Janet Loesel Sitar. We all are indebted to Margaret Knox and Carol Edwards for their skill and good humor in the vital tasks of administration, registration, and reception and to Angie Soonawala and Gisele Wilson for compiling the Proceedings. Special acknowledgment is reserved for Dr. Dan Meneley, Chief Engineer for AECL, for presenting his excellent invited paper to open the workshop.

I wish to further acknowledge the Program Committee for their thoughtful reviews of the papers, command of their sessions, and drafting the summary and conclusions before they left Winnipeg. Special mention is reserved for Jurgen Rohde, co-chairman, whose charm and intellect were ever present throughout the organization and outcome of the Workshop. Finally, on behalf of the organizers, the Program Committee, and CAM, I extend thanks to Jacques Royen, Secretariat to CSNI for his professionalism and enduring serenity in guiding all aspects of the Workshop.

Grant Koroll

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Mr. Jürgen Rohde (GRS, Germany) - Co-Chairman

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The OECD Nuclear Energy Agency (NEA) was established on 1st February 1958 under the name of the OEEC European Nuclear Energy Agency. NEA membership today consists of all European Member countries of OECD as well as Australia, Canada, Japan, Republic of Korea, Mexico and the United States. The Commission of the European Communities takes part in the work of the Agency.

The primary objective of NEA is to promote co-operation among the governments of its participating countries in furthering the development of nuclear power as a safe, environmentally acceptable and economic energy source.

The NEA Committee on the Safety of Nuclear Installations (CSNI) is an international committee made up of scientists and engineers. It was set up in 1973 to develop and coordinate the activities of the OECD Nuclear Energy Agency concerning the technical aspects of the design, construction and operation of nuclear installations insofar as they affect the safety of such installations. The Committee's purpose is to foster international co-operation in nuclear safety amongst the OECD Member countries.

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EXECUTIVE SUMMARY¹

ES.1 INTRODUCTION

The Workshop on the Implementation of Hydrogen Mitigation Techniques was held in Winnipeg, Manitoba, Canada from 1996 May 13 to 15. It was organized in collaboration with the Whiteshell Laboratories of Atomic Energy of Canada Limited (AECL), Ontario Hydro, and the CANDU Owners' Group (COG). Sixty-five experts from twelve OECD Member countries and the Russian Federation attended the meeting. Thirty-five papers were presented in six sessions:

- accident management and analyses,
- relevant aspects of hydrogen production, distribution and mixing,
- engineering,
- technology,
- possible side-effects and consequences, and
- new designs.

It had become clear towards the end of 1994 that several Member countries had reached conclusions on hydrogen mitigation and were moving into an implementation phase. Discussions in CSNI's Principal Working Group on the Confinement of Accidental Radioactive Releases (PWG4) and Task Group on Containment Aspects of Severe Accident Management (CAM) had led to the conclusion that the time was ripe to organize an international Workshop to review the basis for the strategies that were being considered and to provide details of how the strategies were or would be implemented. CSNI therefore decided to sponsor the Workshop, putting emphasis on catalytic recombiners and igniters.

The objectives of the Workshop were the following:

- to establish the state-of-the-art of hydrogen mitigation techniques, with emphasis on igniters and catalytic recombiners;
- to exchange information on Member countries' strategies in managing hydrogen mitigation, and to establish dialogue as to differences in approach;

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- to determine whether there is now an adequate technical basis for such strategies or whether more work—in which areas—is desirable;
- to exchange information on future plans for implementation of hydrogen mitigation techniques.

These objectives have been met. The Workshop has been most successful with respect to the amount and the quality of the information exchanged, and the depth of the discussions. The highlights of the meeting, and the main conclusions, are summarized below.

ES.2 STATUS AND TRENDS OF IMPLEMENTATION

Hydrogen mitigation systems were installed in several reactor types after the occurrence of the TMI-2 accident. Most small-volume BWR containments were inerted and igniters were installed in Mark III BWRs, multi-unit CANDU stations, and PWR containments with ice condensers. In large dry PWRs, reliance was predominantly on large dilution volumes combined with high design pressures. Continued research, new technology and level 2 PSA studies have prompted further assessments in some countries. As a result new initiatives are being taken to implement hydrogen mitigation measures.

Passive autocatalytic recombiners (PARs) have been installed in one of the Belgian nuclear power plants; the other plants will be equipped before the end of 1997. Strong recommendations have been made in Germany by the GRS and the RSK for the implementation of a hydrogen mitigation system in PWRs with large dry containments; a decision about the techniques to be used will be made during the autumn of 1996. In response, German utilities have indicated their preference for a PAR system supplemented by either the installation of igniters in well chosen locations or post-accident-dilution (PAD) by inert gas injection. In the Netherlands, it has been decided to install recombiners in a PWR of German design. In the future, Canada will propose for sale CANDU 9 plants equipped with recombiners and igniters. Most countries are considering the matter of hydrogen mitigation.

It appears from the Workshop that current trends favour the installation of passive autocatalytic recombiners, with the possible installation of complementary igniters. The installation of PARs is also under discussion for future reactor concepts. The installation of catalytic recombiners is being considered for the AP600 reactor for design basis accident scenarios; for the Advanced CANDU reactor and the European Pressurized Water Reactor it is favoured for all accident scenarios including severe ones.

ES.3 ASSESSMENT OF TECHNIQUES

On techniques of hydrogen mitigation, the established principles are mainly based on hydrogen removal or dilution to non-flammable or less sensitive mixtures. Existing mitigation systems include igniters, passive autocatalytic recombiners (PARs), inertisation and containment filtered venting. They have their specific advantages and drawbacks; for example, igniters could deal with higher hydrogen flow rates than PARs, but they have to be more accurately located and need external power. They should not be necessarily considered as alternative solution to PARs, although they can be used as stand-alone solutions. Redundancy would require they be used in some combinations between them or with PARs, for a better hydrogen control.

ES.3.1 Igniters

Igniters have been well established as a method of preventing damaging burns in nuclear power plants by ensuring ignition near the limits of flammability. Availability of power to the igniters is required for their function. Igniters are at pre-determined, fixed locations, and care must be taken in selecting adequate locations, in a manner that is independent of the accident scenario. Special consideration must be given to situations of hydrogen accumulation as a result of temporary steam-inerted conditions. However, in a detailed geometry of a specific reactor containment compartment optimum placement may differ in different scenarios. Engineering judgement combined with study of the gas transport, mixing and stratification as well as studies of combustion behaviour are necessary to optimize igniter placement and for spark igniters, to optimize spark frequency. To assist these studies, lumped parameter as well as detailed three-dimensional codes are available and were discussed at this workshop. Igniter initiation could be done either automatically or through operator action in the control room on the basis of available hydrogen measurements.

ES.3.2 Passive Autocatalytic Recombiners

Passive autocatalytic recombiners (PARs) are devices that recombine the hydrogen present in the containment without a need for external power or operator action. The practical installed capacities in a large containment building are on the order of 100 kg/h made up by 20-60 PARs (depending on PAR unit size), which accommodates a wide range of release scenarios but does not match the maximum possible hydrogen release rates produced in the worst cases. PARs are already being implemented in some plants (e.g., in Belgium) as a means to improve safety margins for hydrogen combustion in severe accident situations.

The installation of PARs is highly influenced by geometric and operational constraints (access to maintenance areas should remain free, PARs must be accessible for periodic surveillance, ...). Therefore engineering judgement is essential in defining the location of the PARs. For future plants, locations of hydrogen mitigation systems may be optimized at the design stage. In the absence of engineering judgement, detailed code calculations would have to address a large number of hydrogen release locations and release rates, and would inevitably indicate PAR locations which are not feasible from an operational standpoint. Nonetheless, calculations can

provide quantitative assessment of the effectiveness of the selected locations. On the other hand, calculations and experiments presented at the meeting showed that the exact location of a PAR in a compartment was not critical for its performance. The strong convection created by a working PAR effectively mixes the atmosphere.

PARs are seen as a promising alternative to thermal recombiners for DBA hydrogen and are under active investigation to resolve particular questions in that application.

ES.3.3 Post-Accident Inert Gas Injection

Post-Accident Injection is a well established technique to avoid burning loads. Post-accident inertisation, under review for reactor application, is a concept which involves injection of an inert gas like CO₂ in fluidized or gaseous form into containment to inert the gas mixture. Another concept is post-accident dilution by injecting an inert gas like CO₂; it was further demonstrated through tests conducted in a full-scale mockup of the injection equipment. The former concept prevents a burn, whereas the latter limits the additional partial pressure buildup by only injecting enough inert gas to preclude turbulent flame acceleration. This latter concept would be used in combination with another technique, such as igniters or PARs as discussed above. Although both concepts were shown at the Workshop to be feasible, no practical installation has been reported up to now. Engineering work is needed to deal with interfacing problems (such as thermal shock, additional large penetration through the containment wall for liquid CO₂ injection, effective gas distribution inside the containment (for both concepts), etc.). A disadvantage of these concepts could be the more complicated initiation procedure in the absence of automatic initiation since the timing is critical. This places an additional burden on operating staff to perform this accident management function, compared to igniters or PARs, which require no operator action. An advantage of these concepts could be the reported low costs for multiple unit application.

ES.4 STRATEGIC AND ACCIDENT MANAGEMENT ASPECTS

Implementation of mitigation techniques is scenario and design specific. Hydrogen mitigation techniques need to be considered in terms of their specific safety goals, required function, and time frame for operation in the accident. These safety goals may be, prevention of flammable mixtures, or prevention of fast deflagrations and detonations. The function of the technique may be dilution, removal or pre-emptive initiation of combustion. Systems and devices could be optimized for use during "design basis" accidents, recoverable severe accidents or severe accidents leading to long lasting hydrogen production in the ex-vessel phase. This could lead to different requirements concerning the qualification programmes for such devices. Mitigation techniques are not to be viewed as alternatives to one another but rather, they are selected to address a particular foreseeable circumstance. As circumstances change over the course of an accident the "fit" or appropriateness of one or another technique may change. Thus, combinations of techniques can provide diversity of function in the different time frames and thus will provide protection over the full range of circumstances. Further, combinations of

techniques reduce redundancy requirements for a particular technique compared to when used on a stand-alone basis.

To provide assurance on the functionality of PARs, a programme for periodic testing is required due to possible aging effects on the catalytic surface during normal plant operation.

The effect of accident management measures on the performance requirements of mitigation systems and the time frame for their operation should be considered. For example, sprays (and coolers) can adversely affect hydrogen mitigation by sensitizing the mixture due to steam removal; however, they also have a benefit in enhancing mixing (and thus dilution) as shown, for instance, in the series of NUPEC tests and the USNRC sponsored tests at Sandia National Laboratories. Another important consideration is the sensitivity of the timing of such actions on the outcome. Measures that are sensitive to timing of actuation raise questions about requirements for operator action in different scenarios.

ES.5 ANALYSIS

Analytical tools are needed to provide simulations of the accident scenarios. Calculations are used as well to describe the hydrogen release, hydrogen mixing in containment and the behaviours arising from mitigation measures.

The key requirement for the tools is to provide guidance in the selection of type, capacity, numbers and locations for hydrogen control systems as well as the boundary conditions under which the systems will function. The tools are relied upon, as well, to analyze the effectiveness and outcomes of the hydrogen mitigation measures.

The lumped-parameter and three-dimensional codes should be used appropriately, based on their capabilities and limitations. Lumped-parameter codes require relatively short computer time to analyze a scenario. Thus, they can be used to analyze a number of representative scenarios. Three-dimensional codes require extensive computational effort but are very useful for particular plant compartments or complex situations which pose a particular challenge for the lumped-parameter codes. Although both types of computer codes provide valuable information, it should be taken into account that each has limitations. For example, lumped-parameter codes cannot describe complex combustion. Three-dimensional codes lack the validation base of the lumped-parameter codes and the experience in the application. Engineering judgement should be used to get the appropriate simulation and utilization from both the lumped-parameter and three-dimensional codes. It is also engineering judgement that should be used to appropriately interpret the computer code results and use them for the final implementation of the hydrogen combustion control technique (e.g., placement of igniters or PARs).

ES.6 UNCERTAINTIES

Regarding H₂ generation and release rates, large uncertainties still remain for the late stage of in-vessel core degradation, as well as for core reflood and quenching. In addition, combustible gas generation is not well-known for the high temperature stage of MCCI after vessel melt-through and during molten-fuel coolant interactions.

Regarding hydrogen distribution in complex subcompartments, qualified three-dimensional codes can help to optimize the location of igniters. As for hydrogen distribution in large, open containment volumes, lumped parameter codes may be adequate, to identify the main convection loops and guide the positioning of recombiners. A qualified condensation model is needed to reduce uncertainties in applications near walls, particularly in ice condenser containments.

Combustion behaviour in subcompartments is uncertain and depends on geometry, hydrogen and steam concentration and availability (and location) of ignition sources. Depending on geometry and gas concentrations, turbulent deflagration, accelerated flames and transition to a detonation (DDT) cannot be excluded in all cases. Models are still under development for combustion in sub-compartments and will have to be experimentally validated. Currently, a criterion to avoid DDT is available but its qualification needs to be extended in large-scale tests such as the RUT experiments at Kurchatov Institute.

Uncertainties were identified in the accident scenarios, hydrogen distributions, combustion behaviour and performance of PARs and igniters under certain conditions but the uncertainties are not considered to be of a nature as to prevent implementation of hydrogen mitigation measures. Nonetheless, reduction of uncertainties is needed to improve confidence in the effectiveness, robustness and safety benefit of the proposed measures.

ES.7 FUTURE NEEDS

ES.7.1 Technology and Testing

Igniters: The technology of such devices is well established. The work still needed is in qualification of power supply and in the detailed modelling needed to confirm the number and location of igniters.

PARs: The process of catalytic recombination is well established and the remaining issues relate to qualification of the equipment for accident conditions. Although the catalytic materials used in PARs have been shown to remain active in prototypical accident situations, experiments are needed to increase the database to cover a larger spectrum of operating and accident conditions. At the meeting, experimental programmes were presented that will assess the performance of PARs at different temperatures, in the presence of possible poisons and for a combination of aerosols and fission product simulants, and after being wetted by sprays. The possible effects of

aging also remain in need of further assessment. Oxidation of other combustible gases such as CO is not well-established.

Post Inert Gas Injection: The concept is well established, but the technology needs to be engineered to nuclear containment conditions. Issues to be resolved include the engineering approach, interfacing with other plant components and detailed modelling to confirm the adequacy of mixing of the diluent gas.

ES.7.2 Tools and Validation

A number of computer codes are used for simulations of hydrogen combustion and gas mixing in containment.

For gas mixing, a skilled user can make good use of both lumped parameters and three-dimensional codes in representing the containment system or a large-scale experiment. In some instances detailed gas mixing patterns are needed (such as to confirm the location of igniters) but very little validation of three-dimensional mixing has been performed. Large-scale tests are needed to validate condensation phenomena and three-dimensional gas mixing codes. Even with large-scale tests, the skill of the user must be developed in addition to the validity of the code. This is essential if the three-dimensional codes would be used for real-plant application, since no site specific full-scale experiments are likely to be conducted and the engineering judgement of the user is required.

For combustion, both lumped parameter and three-dimensional codes were discussed at the Workshop, where it was generally concluded that lumped parameter codes are limited to slow combustion modes.

Predictive tools are also needed for analysis of other modes of combustion, such as standing flames and transition to detonation. Additional experiments are needed to establish the criteria for standing flame stability and to assess their characteristics (temperature, heat transfer). Information is also needed to establish the conditions from which a transition to detonation could occur.

ES.7.3 Facilities and Demonstration

As substantial uncertainties still remain, facilities continue to be required to obtain the information to resolve these, and code validation exercises are needed to demonstrate that the issues are adequately understood. Facilities needed include: small-and medium-scale combustion facilities for DDT; large-scale combustion facilities for deflagration studies and equipment performance testing; large-scale mixing facilities to validate codes, facilities to obtain data on standing flames, and facilities needed to fully characterize the performance of mitigation equipment under simulated accident conditions.

Some versatile, modern facilities are in place to carry out required tests to resolve identified uncertainties and to complete validation of codes. Some of these were described in the

Workshop. Continued operation, reinstrumentation and reconfiguration of these will be needed for validation of three-dimensional codes.

ES.8 CONCLUSIONS

1. Among Member countries considering new measures, there appears to be a trend toward implementation of passive autocatalytic recombiners (PARs) - possibly supplemented by other measures (igniters or post-accident dilution).
2. PARs are seen as a promising alternative to thermal recombiners for long-term hydrogen control.
3. Practical installed capacities of PARs are on the order of 100 kg/h (provided by 20-60 units of different sizes and positions in containment) which can deal with a wide range of hydrogen release scenarios but still cannot match the maximum possible release rates in the worst cases.
4. Locations of PARs back-fitted to existing plants are largely influenced by geometric and operational constraints. Engineering judgement is essential in defining locations for PAR units in the containment.
5. Catalytic materials differ considerably in the different PAR designs. Some are better suited to certain conditions than others. Generally, catalytic materials in PARs have been shown to remain active under prototypical accident conditions. Nonetheless, they require specific qualification testing to cover the spectrum of environmental and operating conditions relevant to the particular application.
6. Dilution (mixing) of hydrogen with available containment air, is an important aspect in hydrogen mitigation. Dilution can be enhanced by engineered systems (fans), configurations that promote natural circulation or by accident management measures (i.e., opening of doors to create flow in ice condenser containments). Analysis of mixing and distribution is an integral part of assessing the effectiveness of further proposed mitigation techniques.
7. Igniter technology is established but qualification of power supply and placement are still issues.
8. Implementation of igniters requires study of gas flow patterns in representative scenarios to optimize location selection.
9. Post Accident Dilution (PAD) concepts were shown to be feasible but no practical installation has been reported up to now.

10. Post Inert Gas Injection is a well established technology, but full engineering to nuclear containment is still under development.
11. Parallel use of lumped-parameter and local application of three-dimensional codes was seen to provide an optimum of computational time and resolution for selected complex cases, in the analysis of implementation measures.
12. For turbulent combustion, three-dimensional combustion codes are required and need validation against experiments.
13. Large-scale tests are needed to validate three-dimensional mixing models.
14. Uncertainties are identified in the scenarios for hydrogen distributions and combustion behaviour but uncertainties are not of a nature as to prevent implementation of hydrogen mitigation measures. Nonetheless, reduction of uncertainties is needed to improve confidence in the effectiveness and safety benefit of the proposed measures.
15. Modern versatile facilities are in place to carry out the required tests to resolve uncertainties and complete validation of codes. Continued operation, reinstrumentation and reconfiguration of these are needed to validate three-dimensional codes.
16. Additional experiments are required to establish standing flame stability and to assess their characteristics.

SOMMAIRE GÉNÉRAL²

SG.1 INTRODUCTION

L'atelier sur la Mise en oeuvre des techniques de mitigation de l'hydrogène s'est tenu à Winnipeg (Manitoba), au Canada, du 13 au 15 mai 1996. Il a été mis sur pied en collaboration avec les Laboratoires de Whiteshell d'Énergie atomique du Canada limitée (EACL), Ontario Hydro et le Groupe des propriétaires de centrales CANDU (COG). Soixante-cinq spécialistes venus de douze pays Membres de l'OCDE et de la Fédération de Russie ont pris part à cette réunion. Trente-cinq communications ont été présentées au cours de six séances :

- analyses et gestion des accidents,
- aspects pertinents de la production, de la répartition et du mélange de l'hydrogène,
- études techniques,
- technologie,
- effets secondaires et conséquences éventuelles, et
- nouveaux modèles.

Vers la fin de 1994, il était devenu évident que plusieurs pays Membres avaient tiré des conclusions en ce qui concerne la mitigation du risque hydrogène et qu'ils étaient en voie de s'engager dans une phase de mise en oeuvre. Les échanges qui avaient eu lieu au sein du groupe de travail principal du CSIN sur le confinement des rejets accidentels de matières radioactives (PWG4) et du groupe de travail sur les aspects liés au confinement lors de la gestion des accidents graves (CAM) avaient permis de tirer la conclusion qu'il était opportun d'organiser un atelier international en vue d'examiner les fondements des stratégies qui étaient envisagées et d'apporter des précisions sur la façon dont ces stratégies étaient ou seraient mises en oeuvre. Le CSIN a donc décidé de parrainer cet atelier, en mettant l'accent sur les recombineurs catalytiques et les igniteurs.

Les objectifs fixés pour cet atelier étaient les suivants :

- établir l'état des connaissances sur les techniques de mitigation du risque hydrogène, en portant une attention particulière aux igniteurs et aux recombineurs catalytiques;

² Publié aussi en anglais sous NEA/CSNI/R(96)9

- échanger de l'information sur les stratégies des pays Membres en ce qui a trait à la gestion de l'hydrogène et engager le dialogue sur les différences au point de vue des méthodes;
- déterminer s'il existe actuellement une base technique convenable pour de telles stratégies ou si des travaux supplémentaires – et dans quels domaines – sont souhaitables;
- échanger de l'information sur les projets futurs de mise en oeuvre des techniques de mitigation du risque hydrogène.

Ces objectifs ont été atteints. L'atelier a donné d'excellents résultats en ce qui concerne la quantité et la qualité de l'information échangée et à l'envergure des discussions. Les points marquants de la réunion et les principales conclusions sont résumés ci-après.

SG.2 ÉTAT ACTUEL ET TENDANCES DE LA MISE EN OEUVRE

Des systèmes de mitigation du risque hydrogène ont été mis en place dans des réacteurs de types divers après l'accident de TMI-2. La plupart des enceintes de confinement de petit volume BWR ont été rendues inertes, et on a installé des igniteurs dans les BWR Mark III, dans les centrales CANDU multitranches et dans les enceintes de PWR à condenseur à glace. Pour les grands PWR à enceinte de confinement sous atmosphère sèche, on se fie surtout aux grands volumes de dilution combinés à de hautes pressions de calcul. La poursuite des recherches, les nouvelles techniques et les études d'analyse de sûreté préliminaire (PSA) de niveau 2 ont incité quelques pays à faire d'autres évaluations. En conséquence, de nouvelles initiatives sont prises pour mettre en oeuvre des mesures de mitigation du risque hydrogène.

Des recombineurs catalytiques passifs (RCP) ont été installés dans une des centrales nucléaires belges; les autres centrales en seront équipées avant la fin de 1997. En Allemagne, la GRS et la RSK ont recommandé vivement la mise en oeuvre d'un système de mitigation du risque hydrogène dans les PWR dotés de grandes enceintes de confinement sous atmosphère sèche. Une décision sur les techniques à utiliser sera prise à l'automne 1996. En réponse, les compagnies d'électricité allemandes ont indiqué leur préférence pour un système RCP complété par l'installation d'igniteurs à des endroits bien choisis ou par la dilution post-accidentelle (DPA) par injection de gaz inerte. Aux Pays-Bas, il a été décidé d'installer des recombineurs dans un PWR de conception allemande. À l'avenir, le Canada offrira sur le marché des centrales CANDU 9 équipées de recombineurs et d'igniteurs. La plupart des pays étudient la question de la mitigation du risque hydrogène.

Il ressort de cet atelier que les tendances actuelles privilégient l'installation de recombineurs catalytiques passifs, avec installation éventuelle d'igniteurs complémentaires. On examine aussi l'installation de RCP dans les modèles de réacteurs futurs. On envisage l'installation de recombineurs catalytiques pour le réacteur AP600 pour les accidents de référence; elle est

privilégiée pour le réacteur CANDU avancé et le réacteur à eau sous pression européen (*European Pressurized Water Reactor*) pour tous les scénarios d'accident y compris les accidents graves.

SG.3 ÉVALUATION DES TECHNIQUES

En ce qui a trait aux techniques de mitigation du risque hydrogène, les principes établis sont fondés principalement sur l'extraction de l'hydrogène ou sa dilution en mélanges ininflammables ou moins sensibles.

Les méthodes de mitigation existantes comprennent les igniteurs, les recombineurs catalytiques passifs (RCP), l'inertisation et l'éventage filtré de l'enceinte de confinement. Elles présentent toutes des avantages et des inconvénients; par exemple, les igniteurs fonctionnent à des débits d'hydrogène supérieurs à ceux des RCP, mais ils doivent être placés avec plus de précision et nécessitent une source d'alimentation électrique extérieure. Ils ne devraient pas être considérés nécessairement comme solution de rechange aux RCP, bien qu'ils puissent être utilisés en autonomie. La redondance exigerait qu'ils soient utilisés sous une forme ou une autre combinés les uns aux autres, ou avec des RCP, pour mieux maîtriser le risque hydrogène.

SG.3.1 Igniteurs

L'utilisation d'igniteurs s'est imposée comme une méthode de prévention de combustions préjudiciables en assurant l'inflammation au voisinage des limites d'inflammabilité. Une source d'alimentation électrique est nécessaire à leur fonctionnement. Ils sont placés à des endroits fixes prédéterminés, et il faut choisir les emplacements convenables avec le plus grand soin, d'une manière qui ne dépende pas du scénario d'accident. Une attention spéciale doit être réservée aux situations où l'hydrogène s'accumule dans des conditions temporaires d'inertage par la vapeur. Toutefois, dans la géométrie détaillée d'un compartiment particulier d'une enceinte de confinement, l'emplacement optimal peut varier d'un scénario à l'autre. Un jugement technique fondé allié à une étude des régimes d'écoulement des gaz est nécessaire pour optimiser l'emplacement des igniteurs et, dans le cas des igniteurs à étincelle, pour optimiser la fréquence des étincelles. Afin de faciliter ces études, on dispose de codes d'analyse à paramètres regroupés et de codes de calcul tridimensionnels détaillés qui ont été examinés au cours de cet atelier. L'activation des igniteurs pourrait être faite soit automatiquement soit par l'intervention de l'opérateur dans la salle de commande en se fiant aux mesures disponibles de l'hydrogène.

EG.3.2 Recombineurs Catalytiques Passifs

Les recombineurs catalytiques passifs (RCP) sont des dispositifs qui recombinent l'hydrogène présent dans l'enceinte de confinement sans faire appel à une source d'alimentation électrique extérieure ou à une intervention de l'opérateur. Les capacités utiles recherchées pour une grande enceinte de confinement sont de l'ordre de 100 kg/h, et sont assurées par 20 à 60 RCP, selon

leur taille, ce qui répond à un grand éventail de scénarios, mais ne correspond pas aux vitesses maximales d'arrivée d'hydrogène atteintes dans les pires cas. On met déjà en oeuvre des RCP dans quelques centrales (p. ex., en Belgique) comme moyen d'améliorer les marges de sûreté pour la combustion de l'hydrogène dans les situations d'accident grave.

L'installation des RCP dépend fortement des contraintes géométriques et d'exploitation (p. ex., l'accès aux aires d'entretien doit demeurer libre, les RCP eux-mêmes doivent être accessibles en vue de la surveillance périodique . . .). Par conséquent, un jugement technique fondé est essentiel pour définir leur emplacement. Dans les centrales futures, l'emplacement des systèmes de mitigation du risque hydrogène peut être optimisé à l'étape de la conception. En l'absence de jugement technique fondé, il faudrait que les codes de calcul détaillés englobent un grand nombre de points et de vitesses d'arrivée de l'hydrogène; ils désigneraient inévitablement des emplacements de RCP qui ne sont pas convenables du point de vue de l'exploitation. Malgré tout, les calculs peuvent fournir une évaluation quantitative de l'efficacité des endroits choisis. Par ailleurs, les calculs et les expériences effectués qui ont été présentés lors de la réunion ont montré que l'emplacement exact d'un RCP dans un compartiment n'est pas crucial pour son bon fonctionnement. La forte convection créée par le fonctionnement d'un RCP mélange efficacement les gaz dans l'atmosphère.

Les RCP sont perçus comme une solution de remplacement prometteuse aux recombineurs thermiques de l'hydrogène dans les accidents de référence et font l'objet d'études intenses en vue de résoudre certaines questions particulières à cette application.

SG.3.3 Injection Post-Accidentelle de Gaz Inerte

L'injection post-accidentelle est une technique bien établie pour éviter les chargements dus à la combustion. L'inertisation post-accidentelle est un concept à l'étude pour les réacteurs qui consiste à injecter un gaz inerte comme le CO_2 sous forme gazeuse ou fluidisée dans l'enceinte de confinement afin de rendre inerte le mélange de gaz. Un autre concept est la dilution post-accidentelle par injection d'un gaz inerte comme le CO_2 ; sa démonstration a été poussée plus avant lors d'essais effectués dans une maquette en grandeur nature de l'équipement d'injection. Le premier concept permet d'empêcher une combustion, tandis que l'autre permet de limiter le pic de pression partiel supplémentaire en n'injectant qu'une quantité suffisante de gaz inerte pour empêcher l'accélération turbulente de la flamme. Ce dernier concept serait utilisé en combinaison avec une autre méthode, telle que des igniteurs ou des RCP comme on l'a vu ci-dessus. Bien que l'on ait montré lors de l'atelier que les deux concepts étaient réalisables, aucune application pratique n'a été mentionnée jusqu'à présent. Des études techniques sont nécessaires en vue d'analyser les problèmes d'interface (tels que le choc thermique, une pénétration importante supplémentaire à travers la paroi de l'enceinte pour l'injection de CO_2 liquide, la répartition effective du gaz dans l'enceinte de confinement (pour les deux concepts), etc.). Un inconvénient de ces concepts pourrait être la procédure de déclenchement plus compliquée en l'absence de déclenchement automatique puisque le moment du déclenchement est d'une importance cruciale. Cela place une charge supplémentaire sur le personnel d'exploitation, qui doit remplir cette fonction de gestion lors d'un accident, comparativement aux igniteurs ou aux RCP, qui n'exigent aucune intervention de la part de l'opérateur. Un

avantage de ces concepts pourrait résider dans les faibles coûts mentionnés pour les applications dans des tranches multiples.

SG.4 ASPECTS STRATÉGIQUES ET DE GESTION DES ACCIDENTS

La mise en oeuvre des techniques de mitigation est particulière à chaque concept et à chaque scénario. Le choix des méthodes de mitigation du risque hydrogène doit tenir compte des objectifs de sûreté particuliers, de la fonction à remplir et du moment de leur déclenchement à la suite de l'accident. Ces objectifs de sûreté peuvent être la prévention des mélanges inflammables ou la prévention des déflagrations et des détonations rapides. La fonction de la technique employée peut être la dilution, l'élimination ou l'inflammation volontaire. Les systèmes et les dispositifs pourraient être optimisés pour servir lors d'accidents "de référence", lors d'accidents graves surmontables ou lors d'accidents graves menant à la production d'hydrogène pendant une longue période dans la phase hors cuve. Cela pourrait se traduire par des exigences différentes relatives aux programmes de qualification de tels dispositifs. On ne doit pas considérer les techniques de mitigation comme des solutions de remplacement de l'une par l'autre, mais on doit plutôt les choisir en fonction de conditions prévisibles particulières. À mesure que les circonstances changent au cours d'une séquence accidentelle, l'efficacité ou la convenance de l'une ou l'autre technique peut diminuer. Ainsi, le recours à une combinaison de techniques peut offrir une diversité de fonctions tout le long de la séquence accidentelle et donc assurer une protection maximale dans toute la plage de circonstances. En outre, le recours à une combinaison de techniques réduit le degré de redondance nécessaire pour une technique particulière comparativement à l'utilisation de cette technique en autonomie.

Un programme d'essai périodique est nécessaire pour garantir le bon fonctionnement des RCP en raison des effets de vieillissement éventuels de la surface catalytique au cours de l'exploitation normale d'une centrale.

Les répercussions des mesures de gestion des accidents sur les performances exigées des systèmes de mitigation et sur le moment de leur déclenchement devraient être prises en compte. Par exemple, les systèmes d'aspersion (et les refroidisseurs) peuvent avoir un effet négatif sur la mitigation de l'hydrogène en rendant sensible le mélange par extraction d'hydrogène; en revanche, ils jouent également un rôle positif en augmentant le mélange (et par conséquent la dilution) comme démontré, par exemple, dans la série d'essais de NUPEC et les essais patronnés par la Commission de réglementation nucléaire des Etats-Unis aux Laboratoires nationaux Sandia. Un autre point à examiner est l'importance de l'étalement dans le temps de ces interventions pour les résultats obtenus. Les méthodes dont l'efficacité dépend du moment de leur déclenchement soulèvent des questions quant aux exigences à imposer à l'intervention de l'opérateur dans divers scénarios.

SG.5 ANALYSE

Des outils analytiques sont nécessaires pour permettre d'effectuer des simulations de scénarios d'accident. De plus, on utilise des calculs pour représenter la production d'hydrogène, le mélange d'hydrogène dans l'enceinte de confinement et les comportements résultant des mesures de mitigation.

La première aptitude que doivent posséder ces outils est de servir de guide dans le choix du type, de la capacité, du nombre et de l'emplacement des dispositifs de mitigation et des conditions aux limites dans lesquelles ces dispositifs fonctionneront. On se fie également à ces outils pour analyser l'efficacité et les résultats des mesures de mitigation du risque hydrogène.

Les codes de calcul tridimensionnels et à paramètres regroupés devraient être utilisés de manière appropriée, en fonction de leurs capacités et de leurs limites. Les codes de calcul à paramètres regroupés demandent relativement peu de temps machine pour analyser un scénario. Par conséquent, on peut les utiliser pour analyser un certain nombre de scénarios représentatifs. Les codes de calcul tridimensionnels demandent beaucoup de temps de calcul, mais ils sont très utiles pour les compartiments particuliers de la centrale ou les situations complexes qui posent un problème spécial aux codes de calcul à paramètres regroupés. Ces deux types de codes de calcul fournissent des informations précieuses, mais il faut tenir compte des limites de chacun. Par exemple, les codes à paramètres regroupés ne peuvent pas représenter une combustion complexe. Les codes tridimensionnels n'ont pas la base de validation qu'ont les codes à paramètres regroupés ni l'expérience d'application. On fera appel au jugement technique fondé pour utiliser de façon optimale les codes tridimensionnels et les codes à paramètres regroupés. Aussi, le jugement technique fondé permettra d'interpréter convenablement les résultats des codes de calcul et de les utiliser pour la mise en oeuvre définitive de la technique de gestion de la combustion de l'hydrogène (p. ex., choix de l'emplacement d'igniteurs ou de RCP).

SG.6 INCERTITUDES

En ce qui a trait aux vitesses de production et d'arrivée de l' H_2 , de grandes incertitudes demeurent encore quant au dernier stade de la dégradation du coeur en cuve, ainsi qu'au sujet du renoyage du coeur et du refroidissement de secours. De plus, la production de gaz combustible n'est pas bien connue au stade haute température de l'interaction corium fondu-béton après rupture de la cuve et pendant les interactions caloporteur-corium fondu.

En ce qui concerne la répartition de l'hydrogène dans les sous-compartiments complexes, des codes tridimensionnels qualifiés sont probablement nécessaires pour optimiser l'emplacement des igniteurs. Quant à la répartition de l'hydrogène dans de grands volumes de confinement ouverts, les codes à paramètres regroupés peuvent convenir pour déterminer les principales boucles de convection et servir de guide pour le positionnement des recombineurs. Il faut un

modèle qualifié de condensation pour réduire les incertitudes dans les applications à proximité des parois, en particulier dans les enceintes à condenseur à glace.

La combustion dans les sous-compartiments se déroule de manière incertaine et dépend de la géométrie, de la concentration d'hydrogène et de vapeur et de la présence de sources d'inflammation. Selon la géométrie (obstacles) et les concentrations de gaz, la déflagration turbulente, les flammes accélérées et la transition déflagration détonation (TDD) ne peuvent pas être exclues dans tous les cas. Les modèles sont encore en cours d'élaboration pour la combustion dans les sous-compartiments et devront être validés expérimentalement. Actuellement, on dispose d'un critère pour éviter la TDD, mais sa qualification doit être étendue à des essais à grande échelle comme les expériences RUT qui ont lieu à l'Institut Kurchatov.

Des incertitudes ont été relevées dans les scénarios d'accident, les répartitions d'hydrogène, le phénomène de combustion et l'efficacité des RCP et des igniteurs dans certaines conditions, mais ces incertitudes ne sont pas considérées comme étant de nature à empêcher la mise en oeuvre de mesures de mitigation du risque hydrogène. Néanmoins, il faut réduire les incertitudes pour rehausser la confiance au point de vue de l'efficacité, de la solidité et des avantages en matière de sûreté des mesures proposées.

SG.7 BESOINS FUTURS

SG.7.1 Technologie et Essais

Igniteurs : La technologie de ces dispositifs est bien établie. Les travaux qui restent à faire sont du domaine de la qualification de l'alimentation électrique et de la modélisation détaillée nécessaire pour confirmer le nombre et l'emplacement des igniteurs.

RCP : Le processus de recombinaison catalytique est bien établi et les questions en suspens se rapportent à la qualification du matériel pour les conditions d'accident. Bien que l'on ait démontré que les matériaux catalytiques utilisés dans les RCP demeurent actifs dans les situations d'accident représentatives, des expériences sont nécessaires pour amplifier la base de données afin de pouvoir envisager un spectre plus étendu de conditions d'exploitation et d'accident. Lors de la réunion, on a présenté des programmes expérimentaux qui permettront d'évaluer l'efficacité des RCP à différentes températures, en présence de poisons éventuels et dans le cas d'une combinaison d'aérosols et de produits de fission simulés, et après avoir été mouillés par des brouillards d'eau pulvérisée. Les effets éventuels du vieillissement demandent aussi d'autres évaluations. L'oxydation d'autres gaz combustibles comme le CO n'est pas bien établie.

Injection post-accidentelle de gaz inerte : Ce concept est bien établi, mais la technologie doit être adaptée aux conditions d'une enceinte de confinement nucléaire. Les questions à résoudre comprennent la méthode technique, l'interface avec d'autres composants de la centrale et la modélisation détaillée nécessaire pour confirmer le bon mélange des gaz diluants.

SG.7.2 Outils et Validation

Un certain nombre de codes de calcul sont utilisés pour les simulations de la combustion de l'hydrogène et du mélange des gaz dans l'enceinte de confinement.

Pour ce qui est du mélange des gaz, un utilisateur averti peut faire bon usage des codes tridimensionnels et à paramètres regroupés pour la représentation du système de confinement ou d'une expérience à grande échelle. Dans certains cas, on a besoin de connaître précisément les régimes de mélange de gaz (comme pour confirmer l'emplacement des igniteurs), mais très peu de validations de modèles de mélange en trois dimensions ont été réalisées. Des essais à grande échelle sont nécessaires pour valider les phénomènes de condensation et les codes de calcul de mélange des gaz en trois dimensions. Même dans le cas des essais à grande échelle, la compétence de l'utilisateur doit être perfectionnée en plus de la validité du code proprement dit. Cela est essentiel si les codes de calcul tridimensionnels sont utilisés pour des applications à des centrales réelles, étant donné qu'il est peu probable que des expériences vraie grandeur propres à un site soient réalisées et que l'utilisateur devra faire appel à son jugement technique.

Quant à la combustion, les codes de calcul tridimensionnels et à paramètres regroupés ont été examinés lors de l'atelier, et on a généralement conclu que les codes à paramètres regroupés étaient restreints aux modes de combustion lente.

Des outils de prévision sont aussi nécessaires pour l'analyse d'autres modes de combustion, comme les flammes permanentes et la transition déflagration détonation. D'autres expériences sont nécessaires pour établir les critères de stabilité des flammes permanentes et évaluer les caractéristiques (température, transfert de chaleur). On a aussi besoin d'informations pour établir les conditions initiales d'une transition déflagration détonation.

SG.7.3 Installations et Démonstration

Puisque des incertitudes considérables subsistent, on a encore besoin d'installations pour obtenir les informations visant à les résoudre, et les exercices de validation de programme sont nécessaires pour démontrer que l'on saisit bien les problèmes. Les installations nécessaires comprennent : des installations de combustion à petite et moyenne échelle pour étudier la TDD; des installations de combustion à grande échelle destinées aux études de déflagration et aux essais de performances du matériel; des installations de mélange à grande échelle pour valider les codes de calcul, des installations pour obtenir des données sur les flammes permanentes, et des installations nécessaires pour caractériser intégralement les performances des dispositifs de mitigation dans des conditions d'accident simulées.

Quelques installations modernes polyvalentes sont en place pour effectuer les essais nécessaires dans le but de résoudre les incertitudes relevées et de réaliser la validation des codes de calcul. Quelques-unes d'entre elles ont été évoquées lors de l'atelier. La validation des codes de calcul tridimensionnels exigera que l'on en poursuive l'exploitation, la réinstrumentation et la reconfiguration.

SG.8 CONCLUSIONS

1. Parmi les pays Membres qui envisagent de nouvelles mesures, il semble y avoir une tendance à la mise en oeuvre de recombineurs catalytiques passifs (RCP)—complétée éventuellement par d'autres mesures (igniteurs ou dilution post-accidentelle).
2. Les RCP sont perçus comme des solutions de rechange prometteuses aux recombineurs thermiques pour la gestion à long terme de l'hydrogène.
3. La capacité installée utile des RCP est de l'ordre de 100 kg/h (assurée par 20 à 60 appareils de différentes tailles placés en divers points dans l'enceinte) et répond à un grand éventail de scénarios de production d'hydrogène, mais ne correspond pas encore aux vitesses de production éventuelles maximales dans les pires cas.
4. Les emplacements des RCP installés après coup dans les centrales existantes dépendent grandement de la géométrie et des contraintes opérationnelles. Le jugement technique est essentiel pour définir ces emplacements.
5. Les matériaux catalytiques diffèrent considérablement dans les différentes conceptions de RCP. Certains conviennent mieux à des conditions données que d'autres. D'une manière générale, on a démontré que les matériaux catalytiques des RCP demeurent actifs dans les conditions d'accident représentatives, mais des expériences spécifiques de qualification sont nécessaires pour englober le spectre de conditions environnementales et de conditions d'exploitation relatives à cette application particulière.
6. La dilution (le mélange) de l'hydrogène avec l'air disponible dans l'enceinte de confinement est un aspect important de la mitigation de l'hydrogène. La dilution peut être augmentée par des dispositifs techniques (ventilateurs), des configurations qui favorisent la circulation naturelle ou des mesures de gestion de l'accident (p. ex., l'ouverture de portes afin de créer un écoulement dans les enceintes à condenseur à glace). L'analyse du mélange et de la répartition fait partie intégrante de l'évaluation de l'efficacité des nouvelles techniques de mitigation proposées.
7. La technique des igniteurs est établie, mais la qualification de l'alimentation électrique et le choix des emplacements présentent encore des problèmes.
8. La mise en oeuvre des igniteurs exige une étude des régimes d'écoulement des gaz dans des scénarios représentatifs pour optimiser les choix d'emplacement.
9. On a démontré que les concepts de dilution post-accidentelle (DPA) étaient réalisables, mais aucune application pratique n'a été mentionnée jusqu'à maintenant.

10. L'injection post-accidentelle de gaz inerte constitue une technique bien établie, mais des études complètes sont encore nécessaires pour son application à une enceinte de confinement nucléaire.
11. L'utilisation parallèle des codes de calcul à paramètres regroupés et l'application locale des codes de calcul tridimensionnels ont semblé offrir une solution optimale en matière de temps de calcul et de résolution pour les cas complexes choisis, dans l'analyse des mesures de mise en oeuvre.
12. Pour ce qui est de la combustion turbulente, des codes de calcul tridimensionnels sont nécessaires et exigent une validation fondée sur des expériences.
13. Des essais à grande échelle sont nécessaires pour valider les modèles de mélange tridimensionnels.
14. On a relevé des incertitudes dans les scénarios de répartition de l'hydrogène et de combustion, mais les incertitudes ne sont pas de nature à empêcher la mise en oeuvre de mesures de mitigation du risque hydrogène. Néanmoins, la réduction des incertitudes est nécessaire pour rehausser la confiance au point de vue de l'efficacité et des avantages en matière de sûreté des mesures proposées.
15. Des installations modernes polyvalentes sont en place pour effectuer les essais nécessaires dans le but de résoudre les incertitudes relevées et d'achever la validation des codes de calcul. La validation des codes de calcul tridimensionnels exigera que l'on en poursuive l'exploitation, la réinstrumentation et la reconfiguration.
16. D'autres expériences sont nécessaires pour établir la stabilité des flammes permanentes et évaluer leurs caractéristiques.

OECD WORKSHOP ON THE IMPLEMENTATION OF HYDROGEN MITIGATION TECHNIQUES

OPENING REMARKS

Jacques Royen
OECD Nuclear Energy Agency

It is a great pleasure to welcome you to this Workshop on the Implementation of Hydrogen Mitigation Techniques on behalf of the Organisation for Economic Co-operation and Development (OECD), the OECD Nuclear Energy Agency (NEA) and its Committee on the Safety of Nuclear Installations (CSNI). I am very pleased to see all of you here but I want to welcome particularly Dr. S. Dorofeev from the Russian Federation, a country not Member of the OECD.

This Workshop is organised in collaboration with the Whiteshell Laboratories of AECL, Ontario Hydro and the CANDU Owners Group. We thank them very much for their hospitality and the excellent arrangements made. These will effectively contribute to the success of the meeting. Our gratitude is due more particularly to Mr. Grant Koroll, Co-Chairman of the Workshop, who has spent much time and effort on the organisation of the Workshop, on the practical arrangements as well as on the technical programme. Mr. Koroll's staff has worked very hard to make this meeting a success, and their work will continue throughout the week.

Our thanks are of course due to the members of the Programme Committee, who will also act as Session Chairmen: Mr. De Boeck, Mr. Duco, Mr. Fluke, Prof. Fisher, Dr. Frid, Dr. Malliakos and Mr. Rhode. One of their tasks will be to write during the Workshop a brief summary of the highlights and major points of interest of their session. Another task will be to prepare in a joint session, held immediately after the Workshop, a brief note on the Conclusions, and possible Recommendations which can be drawn from the meeting.

Finally, I want to thank all the authors who contributed papers to the Workshop. The Programme Committee found these were generally of high quality, and expects this meeting to make a remarkable contribution to the advancement of the state-of-the-art of hydrogen mitigation techniques.

Let me emphasize the essential importance of discussion periods in specialist meetings and workshops sponsored by the CSNI. This is where progress is made, this is what makes these meetings highly efficient. In your presentations, please make sure you leave enough time - at least ten minutes - for discussions.

I wish you a most useful meeting.

OECD WORKSHOP ON THE IMPLEMENTATION OF HYDROGEN MITIGATION TECHNIQUES

CLOSING REMARKS

Jacques Royen
OECD Nuclear Energy Agency

At the end of this OECD Workshop on the Implementation of Hydrogen Mitigation Techniques, I want to thank you all for your contributions through your presentations and your participation in the discussions. The meeting has been most interesting and useful, and is unanimously recognized as very successful. The exchange of information on national positions and strategies on hydrogen management has been very open, as well as the exchange on future plans. It has clarified positions already taken or still under discussion, and helped progress on the understanding of phenomena and techniques. It has identified remaining issues.

As I said in my opening remarks, the Conclusions of the Workshop will be drafted this week by the Programme Committee, for the benefit of the Committee on the Safety of Nuclear Installations (CSNI) and the Committee on Nuclear Regulatory Activities (CNRA). They will be sent to you for comments. The Conclusions will assist Member countries' deliberations on solutions to be given to hydrogen burn issues.

Let me spend a couple of minutes on explaining how this activity fits in the CSNI Programme of Work. First of all, the Conclusions of this Workshop will complete a report on Severe Accident Management Implementation drafted last year by CSNI's Senior Group of Experts on Severe Accident Management (SESAM). This group had agreed that several areas still remained where further understanding of the phenomena could strengthen the robustness of severe accident management strategies and provide additional confidence in the mitigative actions or decisions contemplated for severe accident management. These areas were :

- in-vessel core debris coolability and reactor pressure vessel integrity;
- ex-vessel flooding to provide in-vessel core debris cooling, and
- ex-vessel debris coolability and fuel/coolant interactions.

SESAM had also identified a fourth area as important for severe accident management: the issue of hydrogen generation, distribution, combustion and recombination. However, extensive research work had already been carried out in this area, and current investigations were increasingly concerned with details of plant and design specific implementation of hydrogen management in the containment. SESAM had therefore agreed to leave it to the Winnipeg Workshop to draw suitable conclusions in this area.

While the SESAM report focused on severe accident management and was essentially restricted to accident management associated with existing plants, another report prepared by a Follow-up Group to CSNI's Senior Group of Experts on Safety Research (SESAR) covered all safety

research aspects, including those related to the hardening of knowledge deemed necessary to ensure and improve safety and those related to the strengthening of the regulatory base, for current and future plants. There was agreement among the Group members on the global aspects of hydrogen generation, transport, combustion, control and analysis methods but there was no consensus regarding the accuracy needed in assessment of local hydrogen concentrations and combustion processes, and the effectiveness of certain hydrogen control techniques. The Group highlighted the need for further research on hydrogen production during recovery actions of a partly damaged core, passive recombiner performance over a wide range of environmental conditions, improvement and validation of analytical methods for evaluating hydrogen mixing and combustion.

Another activity sponsored by the CSNI is a Writing Group preparing a State-of-the-Art report on containment thermal-hydraulics and hydrogen distribution.

Upon closing this Workshop, I want to thank again the Whiteshell Laboratories of AECL for organising the meeting in collaboration with NEA and for their hospitality, and Ontario Hydro and the CANDU Owners' Group for their support. I express again our gratitude to Mr. Grant Koroll, and to his staff who helped with many practical tasks during the sessions. Special thanks are due to Mrs. Margaret Knox and Mrs. Carole Edwards, whose kindness and excellent work before and during the Workshop ensured the smooth and efficient march of the meeting.

I wish you an interesting visit of the Whiteshell Laboratories tomorrow, and a pleasant journey back home.

SUMMARIES OF THE SESSIONS

INTRODUCTORY SESSION

Chairman: G.W. Koroll, AECL

The Workshop was opened with welcoming and introductory remarks from the hosting Chairman, G.W. Koroll; the Canadian CSNI Member and Director of Reactor Safety Research, Dr. L. A. Simpson; and the Chairman of the Task Group on Containment Aspects of Severe Accident Management (CAM), Mr. J. Duco.

The invited paper was presented by Dr. D. A. Meneley, Chief Engineer, AECL. Dr. Meneley's paper was titled, 'A Designer's View of Hydrogen Mitigation'. The paper reviewed the concepts of design basis and severe accidents as they apply to the design process and went on to examine the safety goals, the design issues and regulatory implications with respect to hydrogen and containment integrity. The paper emphasized the necessity of hydrogen mitigation in terms of the absolute requirement to maintain containment integrity during accidents. In discussion of hydrogen measures, Dr. Meneley identified the key design issues being availability (i.e., qualification for service), safety orientation (i.e., operating in a way that consistently improves safety in foreseeable scenarios), function (the devices demonstrably perform their intended function and are engineered in appropriate ways into the containment) and cost. To guide the selection of appropriate measures for hydrogen management, the principles of defence-in depth were invoked with the examples illustrating how independent diverse and redundant measures can improve safety margins.

An overview of the Canadian Program on Hydrogen and the particular issues related to hydrogen in CANDU designs was presented by Mr. R.J. Fluke, Ontario Hydro Nuclear. Mr. Fluke described the scenarios leading to hydrogen production, the different CANDU containment designs, the hydrogen control strategies adopted, the codes used in the analysis and the research program in support of hydrogen issues carried out at AECL through the CANDU Owners Group (COG). Multi-unit containment designs operated by Ontario Hydro use a combination of forced mixing and ignitors for hydrogen mitigation. Single-unit containment designs rely on dilution of the hydrogen with the large volume of containment air. The GOTHIC code is used to analyse mixing, distribution, recombination and combustion of hydrogen. An ambitious plan to validate the three-dimensional combustion and mixing models in the containment codes is underway. Research support of hydrogen issues is provided by COG in programs addressing three-dimensional mixing, deflagration, deflagration to detonation transition, standing flames and catalytic recombiners. Highlights include a large-scale, instrumented, insulated combustion test facility, an large-scale gas mixing test facility and a unique catalytic recombiner technology that resists water and water vapour at low temperatures.

At completion of the session, Dr. J Royen, OECD Nuclear Energy Agency, provided additional welcoming remarks, thanked the organizers and the Program Committee for their good work and offered guidance regarding the conduct of the workshop and the purpose it was expected to serve for CSNI.

SESSION I: ACCIDENT MANAGEMENT AND ANALYSES

Chairman: J. Duco, IPSN

Six papers have been presented in Session I on accident management and analysis.

Paper one, entitled "Hydrogen Mitigation Systems - A Canadian Regulatory Perspective," and presented by Dr. J.K. Khosla (Atomic Energy Control Board Canada), is a discussion paper to examine the regulatory requirements that may be necessary for the design, operation and maintenance of the hydrogen mitigation systems. These systems, if they are deemed necessary to maintain the integrity of the containment system, may be considered to be a part of the containment systems and therefore should meet all the relevant requirements given in AECB regulatory Document R-7, which specifies the requirements for containment systems for CANDU nuclear power plants. Some additional requirements, which are specific to these systems, have also been included. These requirements relate to a systematic examination of the hazards of hydrogen, the design basis for the mitigation systems, their functional and design requirements, analytical support to justify their selection, and operating and testing requirements; they are intended for the new reactors in Canada, whereas their application to existing reactors will be subjected to practicability. It is recognized that there may be a number of difficulties in meeting all these requirements. The analysis tools have not yet been fully developed and their validation will require considerable additional research work. While the analytical and experimental research effort is in progress, the decisions with regard to the design (the number and locations of the igniters and/or recombiners) of the mitigation system will have to be made. It is also worth noting that the recombiners, though currently looking very promising, are in the early stage of their development. Given all these uncertainties, it is clear that in making those decisions, a fair bit of engineering judgement will have to be used, and it should be based on the current state of knowledge and prudence. The requirements for severe accidents have not yet been developed in Canada, but it is anticipated that the design of the hydrogen mitigation system would be such that future requirement can be accommodated.

The second paper, entitled "Rationale for the Implementation of Hydrogen Mitigation Techniques on French PWRs", and presented by Mr. Duco from the Institut de Protection et de Sûreté Nucléaire (IPSN) in France, first identified the IPSN technical needs to realistically assess the hydrogen issue in the containment under severe accident conditions. The physical phenomena concerned are highlighted, as well as major uncertainties affecting current assessment tools, which results in an objective-oriented research prioritization. The IPSN hydrogen research program is then described, along with national and international cooperations. Regarding the hydrogen generation and generation rate for the severe accident scenarios of concern on French PWRs, a comprehensive, state-of-the-art report, still in draft form, has been worked out. It is apparent that efforts are still to be made for a proper assessment of hydrogen generation for the late in-vessel core degradation phase and reflooding. Regarding the hydrogen distribution in the containment and deflagration, IPSN has launched the elaboration of the TONUS code, combining lumped parameter and three-dimensional aspects: in particular, steam condensation models onto the walls, in presence of non-condensable gases, and condensation

models on falling droplets of a water high-density spray are expected to be qualified in the TOSQAN test facility at IPSN Saclay. Concerning the deflagration-to-detonation transition, IPSN participation in the RUT program, in collaboration with FZK, USNRC and the Kurchatov Institute, which is expected to provide shortly comprehensive data for establishing a criterion excluding DDT. Simultaneously, IPSN has launched an experimental program, H2PAR, in its Cadarache laboratories, to check that some passive autocatalytic recombiners, which could be proposed by the utility for installation on French units, remain efficient under severe accident conditions, when exposed to a representative variety of fission products and other core material simulants: the first results are expected in 1997, but tests are scheduled to be continued in 1998 and beyond.

The third paper, entitled "Recent Work in Sweden Concerning the Hydrogen Issue at Ringhals PWR" and presented by Dr. Veine Gustavsson from Vattenfall Energisystem AB in Sweden, gives an overview of the work in the last three years. Based on a PSA level 1 study providing the core damage frequency in different scenarios, a study has been performed in order to estimate the probability of containment failure due to hydrogen deflagration after a severe accident in Ringhals 3, one of the three, 3-loop W-PWRs at the Ringhals site. Two event-trees have been used, one before reactor vessel failure, the other after such a failure. The calculations were carried out for two cases as regards the input data: best-estimate and conservative data, respectively. In-vessel and ex-vessel hydrogen generation data are provided for six accident sequences, as well as the relevant pressures in the containment resulting from a deflagration.

The probabilities of a hydrogen deflagration before and after reactor vessel failure are provided and the probability of containment failure assumed as a function of the pressure. The probability of failure of the containment is extremely low before vessel melt through. Its value is therefore calculated as the product of the probability of the reactor vessel failure by that of containment failure, given that vessel failure has occurred. The result is less than 1×10^{-7} /year, using conservative assumptions. Such a probability could be strongly reduced by introduction of catalytic recombiners. However, Swedish plants are equipped with filtered venting systems, which could be used for the hydrogen release to the atmosphere.

The fourth paper, entitled "EDF Analysis of the Hydrogen Problem on Present NPPs" and presented by Mr. Guieu from Électricité de France, is a synthesis of the analysis of the French utility in order to answer the following questions concerning the hydrogen risk on the types of PWRs currently in operation: a) is there a need of mitigation measures, and b) what are the most appropriate, if the need is apparent. Regarding the cumulative amount of hydrogen generated for in-vessel arrested severe accident sequences, the amount proposed as a conservative value corresponds to the oxidation of 100% of the zirconium fuel cladding in the active part of the core. Assuming a global hydrogen deflagration, the resulting peak pressure in the containment is calculated in a realistic way, and compared to the "ultimate" containment failure pressure. In conclusion, the installation of catalytic recombiners is under consideration for P'4 and N4 units, as a conservative measure, but such equipment will be effective only after full qualification as regards the efficiency of the devices. Alternative or complementary methods, like the use of igniters or containment atmosphere inerting, are rejected for the time being, whereas a test program on passive autocatalytic recombiners is going on at CEA and IPSN.

The fifth paper, entitled "Hydrogen Management Strategy for the Loviisa NPP," and presented by P. Lundstrom from IVO International Ltd. of Finland, deals with a new hydrogen management scheme developed for the Loviisa ice condenser containment as a part of a comprehensive severe accident management (SAM) strategy. The scheme is based on providing sufficient mixing of the containment atmosphere, effective energy removal from the containment, and controlled removal of hydrogen through passive catalytic recombination. The paper aims at demonstrating how this hydrogen management scheme works for a range of relevant severe accident scenarios. The hydrogen management strategy proposed is based on the following logical steps. First, a global convective loop flow is due to take place in the containment during hydrogen release, if the ice condenser doors are forced open. Second, a maximum tolerable hydrogen molar fraction has been defined, both to avoid local flame acceleration and transition to a detonation, and not to exceed the adiabatic isochoric complete combustion pressure approaching the containment failure pressure. Third, an assessment of the hydrogen generation rate in the core has been carried out, keeping in mind that faster hydrogen generation rates are compensated by enhanced mixing. Ultimately the passive catalytic recombiner system can be dimensioned as a consequence of the above. The convective loop flow in the containment is confirmed by a test on the VICTORIA facility, geometrically similar to Loviisa. For a hydrogen concentration limiting criterion of 9%, and a hydrogen release rate of 0.15 kg/s for 5300 s, it is concluded that a total of 100 m² of inlet recombiner area would be adequate.

The last paper, entitled "MELCOR 1.8.3 Application to NUPEC M-7-1 Test (ISP-35) and Two Hydrogen Severe Accident Scenarios in a Typical PWR Plant," was presented by Mr. Jimenez Garcia from the Polytechnical University of Madrid. A MELCOR nodalisation scheme of the NUPEC facility has been developed for gas distribution and mixing, comprising 33 control volumes, 77 flow paths and 124 heat structures; containment spray nozzles have been introduced. One of the novelties of the MELCOR 1.8.3 version is the inclusion of the water film-tracking model, allowing to simulate the effect that the sprayed water impingement on the containment walls has on the heat transfer. Calculations performed for the NUPEC M-7-1 experiment have confirmed the code ability to make quite a good simulation of this test, which gives some confidence for applying MELCOR 1.8.3 to the study of hydrogen behaviour and distribution under severe reactor accident conditions with the water sprays active. The second part of the study consists in the application of MELCOR 1.8.3 to a typical 2700 MWth 3-loop W-PWR Spanish plant, for a hydrogen producing accident scenario chosen as a station blackout coincident with the seal failure of the three primary system coolant pumps. Two cases have been considered a) the accident progression beyond the reactor vessel failure, with water spray recovery after vessel failure, and b) early injection and water spray system recovery, arresting the accident in the vessel.

SESSION II: RELEVANT ASPECTS OF HYDROGEN PRODUCTION, DISTRIBUTION AND MIXING

Chairman: W. Frid, SKI

The session, which contained four papers, addressed one of the key severe accident phenomena namely mixing and distribution of hydrogen in the reactor containment building. For instance, poor mixing of hydrogen within the containment could lead to a non-homogenous distribution of hydrogen concentration which in turn could result in fast combustion, including detonations, and dynamic, damaging loads on the containment structures. Thus, an assessment of hydrogen transport and distribution within containment is necessary for the assessment of hydrogen threats, as well as for designing adequate accident management strategies.

In the first three papers, the analytical approaches to the hydrogen distribution problem were presented, while the last paper described experimental studies of hydrogen behaviour. The first paper, presented by G. Preusser, Siemens-UWU, Germany described calculations of hydrogen recombination and distribution using lumped parameter codes WAVCO and 3D code FLUTAN. WAVCO analyses for a worst case accident scenario in a KWU 1300 MWe PWR, have shown that in order to avoid flammable gas composition in the upper steam generator compartment a combination of recombiners and dilution of containment atmosphere by carbon dioxide is required. In the second part of this paper, a comparison of hydrogen distribution predicted by WAVCO and FLUTAN was described. The three-dimensional FLUTAN code gave better predictions in containment regions near the source of hot gases. In the regions far away from the source, the WAVCO predictions were acceptable, as one would expect. The future code development plans at Siemens include coupling of WAVCO and FLUTAN in order to optimize calculations by combining advantages of both approaches.

The next paper entitled "Analysis of Mitigating Measures during Steam/Hydrogen Distributions in Nuclear Reactor Containments with the 3D Field Code GASFLOW" was presented by P. Royl from FZK in Germany. A number of code improvements have been recently made. These include implementation of ignitors and the recombiner model which allow integral simulation of transport, combustion and mitigation measures, including "dual-concept", i.e., simultaneous use of ignitors and recombiners. A rather detailed description and discussion of validation and application of the code was presented. These include validation of hydrogen/steam distribution analyses and recombiner model against tests in HDR and Battelle facilities. In addition, scoping analyses of CO₂ inertisation of the containment and simulation of hydrogen recombination in the inner rooms of a German 1300 MWe PWR were presented. It was concluded that code validation process is so far satisfactory and that three-dimensional field codes simulations have become feasible and provide reliable predictions. Computer time requirements are however very significant which prevents application of three-dimensional codes to sensitivity analysis. Another conclusion was that lumped parameter codes can be adjusted and calibrated to include relevant findings from three-dimensional-analyses.

K. Yim from Ontario Hydro Nuclear in Canada presented analysis of post-accident hydrogen mixing behaviour in CANDU Fuelling Machine Vault using another three-dimensional-code, GOTHIC. The objective of this study was to demonstrate by detailed three-dimensional modelling that existing hydrogen mitigation measures in the form of a Hydrogen Ignition System (glow plug igniters) in Ontario Hydro multi-unit CANDU reactor containment are acceptable, since the number and location of igniters were selected based on engineering judgement. The GOTHIC calculations were performed for the fuelling machine vault which is considered to be the compartment of most concern with respect to hydrogen concentrations. Initial conditions for calculations were determined based on mixing experiments and preliminary GOTHIC calculations. The predicted compositions of the gas mixture were compared with ignition limits and it was shown that flammable mixture would not form, except for a brief period during which the mixture composition was just slightly higher than the ignition limit. It was concluded that the existing hydrogen mitigation system is adequate because natural mixing and dispersion prevents formation of sensitive hydrogen mixtures. The discussion of the paper was focussed on initial and boundary conditions for the analyses. The thermalhydraulic and gas mixture conditions in compartments surrounding the vault were obtained from calculations using lumped parameter mode of the GOTHIC code.

The last paper in the session described experimental studies of hydrogen behaviour in ice condenser containments and was presented by P. Lundström from IVO International Ltd. in Finland. Three mixing experiments in the VICTORIA test facility were described and discussed. The VICTORIA facility is a scale model of the Loviisa ice condenser containment with a linear factor of 1/15. The facility is used, in combination with modelling and scaling studies, for experimental studies in support of a new hydrogen mitigation scheme under development for the Loviisa plant. The strategy relies on containment-wide natural circulation to produce effective mixing and slow rise in hydrogen concentrations. At present, the Loviisa plant is equipped with glow plug igniters powered by the emergency diesel generators. The objective of the three experiments presented was to investigate mixing behaviour in the upper containment compartment under strong thermal stratification and under isothermal conditions, as well as containment behaviour with depleted ice condensers. The experiments have shown, among other things, that the overall mixing behaviour of the upper compartment depended on the initial conditions before gas injection, that an initial thermal stratification in the upper compartment was rather stable, and that the absence of ice did not affect the mixing behaviour. During the discussion of the paper, a question was raised if the VICTORIA facility could be used for an ISP on code validation. The facility seems quite suitable for such an exercise considering its geometrical features and good instrumentation. However, modelling the ice condenser seems to be very difficult.

In summary, the session provided a good insight into the status of the state-of-the-art computer codes for mixing analysis and their applications to reactor accidents and experiments. It seems that calculation approach described in the Ontario paper, i.e., combination of the lumped parameter and three-dimensional codes, is a sound one. The idea of coupling the lumped parameter codes with three-dimensional codes, discussed in the Siemens paper, is an interesting one, but during the discussion doubts were expressed if it is feasible due to the complexity of the fluid mechanics involved. Further experimental validation of the three-dimensional computer

codes is needed. The paper on VICTORIA experiments illustrated the fact that a combination of well designed experiments and analytical methods is necessary to resolve the complex problem of hydrogen distribution in the reactor containments.

SESSION III: ENGINEERING

Chairman: M. Fischer, Aerospace Research Establishment, Germany

Seven papers were presented in this session dealing with the engineering aspects of hydrogen management in the containment during LOCA and severe accidents. Among the mitigation measures, treated within the scope of this session are the use of igniters leading to safe ignition within various type of containments and the catalytic recombination of hydrogen and oxygen to produce steam. Other subjects of the presentations include the development and validation of a computer model simulating catalytic recombiners, calculations on their implementation and effectiveness during accident propagation within large containments as well as on first experiences concerning their installation within real containments.

The first paper of this session was presented by Drs. H.C. Kim and J.I. Lee from Korea Institute of Nuclear Safety and dealt with an analysis of hydrogen concentration distribution within a CANDU-6 containment following a LOCA without any emergency core cooling. The code applied for the feasibility study was the normally used code CONTAIN for CANDU PHWR.

The results indicated that the calculated peak hydrogen concentrations were equal to those previously calculated and no hydrogen burn occurred at low hydrogen concentrations. However, improvements could still be achieved by integrating the CANDU-specific layout and safety feature models within the code.

Hydrogen combustion management with igniters during a severe accident at a plant with the ice condenser type of containment was the topic for the second presentation. Severe accident scenarios with an expected large amount of release of hydrogen were analyzed using the code MAAP 3.0. The large amount of hydrogen generated resulted in a severe effect concerning the increase in containment internal pressure during burning while the high generation rate caused problems of hydrogen control. Selection of the locations for igniters will consider the hydrogen mixing behaviour to minimize the resultant pressure increase within the containment.

The third paper on a mechanistic approach to safe igniter implementation for hydrogen mitigation, investigated the bounding hydrogen/steam release sequences with the associated three-dimensional analyses of transport and mixing and the selection of apparently suitable igniter positions by using a new DDT-criterion, based on large scale experimental results. The determination of the detonation potential at the time of first ignition and the modelling of deflagration modes were also carried out. The methodology was implemented into the GASFLOW code and demonstrated for a single room with early and late ignition of the

spreading of a hydrogen-air cloud. The selection of a higher igniter position could lead to early ignition, while the lower positions of igniters resulted in late ignition with high DDT possibility.

The fourth paper of this session described a new combustion test facility which was constructed, instrumented and commissioned at AECL-WL to perform a variety of combustion experiments relevant to hydrogen behaviour in nuclear containments. Operating experiences indicated that the facility was versatile and many geometrical configurations could be achieved. The facility features particularly good control and measurement of initial gas compositions. The preliminary results performed within this large scale vented combustion test facility were also presented within the scope of the presentation.

The fifth paper of this session reported on the development of a catalytic recombiner model implemented in the containment code RALOC MOD4.0. The validation of the model based on experimental results showed that the essential physical parameters like temperature behaviour, reaction rates and gas flows for different types of recombiner could be reproduced fairly well. The overall structure of the model was revealed to be flexible enough to simulate various type of recombiners. The degree of nodalisation of the direct surrounding of the recombiners showed no direct influences on the characteristics of the model under the same thermalhydraulic conditions. One dimensional model for the box-type of recombiner (Siemens and NIS recombiners) could be successfully applied for the post-calculation of experiments.

The sixth paper reported the results of investigations for the implementation of catalytic recombiners in large dry containments in Germany. The effectiveness of such a system was demonstrated by comparing the calculations performed for a specific severe accident sequence without and with the catalytic recombination of hydrogen. Results indicated that only in some limited areas in the containment combustible gas mixtures were formed for a limited time span and later after a lapse of a day from the onset of the accident the catalytic reaction was limited due to oxygen depletion.

The last paper reported on the first experience with the installation of passive autocatalytic recombiners in the Belgian Doel 1 unit. Sizing and the location of the catalytic surface inside the containment was based on a single volume approach combined with engineering judgement. Periodic control through in-service inspection of the catalytic capability was recommended and the test procedures had been prescribed.

SESSION IV: TECHNOLOGY

Chairman: R.J. Fluke, Ontario Hydro Nuclear

Seven papers were presented in the Technology Session of the workshop describing the development, testing and modelling of equipment designed for hydrogen consequence mitigation. Several mitigation devices have been proposed, all of which prevent damaging

hydrogen burns. The methods discussed in this session include post-accident inert gas injection, passive autocatalytic recombiners, and a detonation arrester.

Post-Accident Inert Gas Injection

The objective of post-accident inert gas injection is to either inert the mixture and prevent a burn, or to dilute the hydrogen mixture to change the potential mode of combustion, to prevent a damaging burn. Professor H. Karwat, Technical University of Munich, described the concept of post-accident inertisation of PWR containments by injection of liquid CO₂. Using the containment code RALOC, simulations of liquid CO₂ injection into a LWR containment from the dome area showed that an inertising concentration of 60% could be achieved in 30 minutes. He described the strategy, noting that timing was very important, with the ideal time of matching the rate of steam condensation. Manual actuation from the control room was suggested to ensure the inertisation did not actuate spuriously, too soon, or too late.

B. Eckardt (Manager, Venting and Inertisation Systems at Siemens) followed with a variation on the concept, based on dilution using gaseous CO₂ injection instead of liquid CO₂. One advantage is to avoid the problem of thermal shock associated with liquid CO₂ injection. In the dilution system, CO₂ is injected upward from a low or mid-elevation as a high velocity jet, which helps promote good mixing. The concept appears to be relatively simple from an engineering perspective, and is proposed as a complement to recombiners or igniters. Multi-compartment analyses using MAAP 3.0B were presented to illustrate the effectiveness of the concept. Few recombiners would be needed, it was shown, with the CO₂ injection. The CO₂ mixing with air was further demonstrated through tests conducted in a full scale mock-up of the injection equipment.

Passive Autocatalytic Recombiners (PAR)

Passive Autocatalytic Recombiners are self-actuating, requiring no source of power, and promote natural convection from the heat of recombination.

The AECL hydrogen recombiner development was described by G.W. Koroll, Manager of the Containment Analysis Branch. He began with a history of the special AECL catalysts that came out of a new process for CANDU heavy water production. He then described the requirements for current applications in a reactor containment including "cold-start" capability. The AECL PAR design is based on a ceramic-platinum complex bonded to a stainless steel matrix with a wet-proofing compound, manufactured as plates which fit into a rectangular enclosure. Testing in the Large Scale Vented Combustion Test Facility shows that it removes hydrogen effectively, including cold-startup, and shows good resistance to poisoning and fouling. In the discussion, questions were asked about its potential ignition source and the extent to which iodine would impair its performance. Mr. Koroll explained that the recombiner was intended as a complement to igniters and therefore optimized performance in the non-flammable range. Potential high temperature of the catalyst in the flammable range was not a concern where igniters are already present. However, he explained that a performance trade-off could be made to ensure that it did not pose a potential ignition source. On poisoning, he indicated that

molecular iodine at high, unrealistic concentrations (10^{-5} M) would impair performance, but at expected post-accident concentrations (10^{-8} M), performance is not affected.

Dr. Asimios Malliakos, Project Manager of Hydrogen Research at the USNRC, described confirmatory performance testing of Passive Autocatalytic Recombiners (PARs) which are being considered for ALWRs. Such devices remove hydrogen catalytically, by recombining it with oxygen, without external power. The heat generated by recombination promotes natural circulation to help mix combustible gases in containment. A prototype model was tested in the Surtsey Facility at Sandia National Laboratories, a 90 m³ vessel capable of 1 MPa at 260 K, which is well instrumented. Six tests were described, although only one test has been completed and the results were presented in the workshop. The preliminary conclusions were that the PAR began recombining H₂ with O₂ after low hydrogen concentrations were introduced into the facility which contained dry air. This was indicated by the immediate temperature rise of the catalyst. A convective flow loop was quickly established in the upper half of the Surtsey facility. Little if any mixing occurred in the lower half of the facility.

Dr. Pierre Rongier, a physicist with CEA/IPSN at Cadarache, France, described the new H₂PAR Facility and outlined the test matrix which aims to examine practical performance issues for catalytic recombiner, including the effects of poisons (I₂, CH₃I, CO, organic vapours, sprays and aerosols). He also described some preliminary test results needed to scope the test conditions for the large-scale (8 m³) facility. The first test is planned before year end, 1996.

Dr. Jacques Vendel, also a physicist at IPSN, followed the presentation by Dr. Rongier, and described the detailed modelling of aerosol deposition in a hydrogen catalytic recombiner. Three particle sizes were analyzed: 0.1 μ, 1.0 μ and 10 μ radii. The deposition depended strongly on whether the plate is hot or cold. For a cold plate, thermophoresis and brownian diffusion dominated. For a hot plate, thermophoresis inhibits deposition, although diffusiophoresis was shown to be dominant. The driver for diffusiophoresis was shown to be the gas flux of hydrogen and oxygen toward the plate. A question was raised regarding the steam vapour flow, which would be in the opposite direction from a hot plate, as to the role in impeding aerosol deposition. This effect required some further assessment.

Detonation Arrester

Dr. Manfred Fischer, Director of the Institute for Technical Thermodynamics, German Aerospace Research Establishment, and Professor of Hydrogen Energy Technology at University of Stuttgart, presented a detailed account on modelling the turbulent flame acceleration and detonation quenching and reinitiation. The detailed modelling of such a complex set of phenomena based on computational fluid dynamics codes is fairly recent (due to computer and practical limitations in the past). Dr. Fischer explained the use of these codes in investigating and understanding fundamental interactions, and how they help to clarify the process of predicting the conditions leading to potential detonation. He used a number of comparisons with experiments to demonstrate that a good understanding of detonations is available and offered some possible mitigation strategies. One of these is a detonation arrester, similar in concept to a flame arrester, but as he pointed out, quite different in nature.

The Status of Mitigation Technology

In summary, seven papers were presented that relate to mitigation technology. Two of these involved modelling or testing the concept of post-accident inerting using CO₂ injection; four discussed the development, testing and performance evaluation for passive autocatalytic recombiners, and one provided a detailed review of detonation phenomena and introduced a device to arrest the consequence of a detonation. It would appear that the science of hydrogen mitigation to prevent damaging burns in containment is fairly well advanced, and some practical engineering issues, such as performance under various accident conditions need to be resolved before the technology is validated. This is evident from the engineering nature of the presentations in this session.

SESSION V: POSSIBLE SIDE-EFFECTS AND CONSEQUENCES

Chairman: A. Malliakos, USNRC

In the presentation of "Turbulent Combustion and DDT Events as an Upper Bound for Hydrogen Mitigation Techniques" by Dr. S.B. Dorofeev from the Russian Research Center, Kurchatov Institute, an emphasis was given on the peak overpressures produced by DDT and fast turbulent deflagrations. Criteria were presented that are necessary to have DDT. The criteria use the parameters of detonation cell width and minimum size of a sensitized mixture required for localized explosion to result in a detonation. The criteria were derived and confirmed by a wide spectrum of experimental data from various facilities. Questions were raised from the workshop participants, during the question period at the end of the presentation, that the criteria do not address geometric configurations and in particular do not address the effect of obstacles. A clarification was made that the criteria are necessary for DDT, but if all criteria are met this does not mean that DDT will occur. It was suggested that future work should focus to develop criteria that will lead to DDT.

The experimental work along with the associated analytical efforts on turbulent deflagration was presented by Mr. B. Durst from the Technical University of Munich, Germany in a presentation titled "Interaction of Turbulence Deflagration with Representative Flow Obstacles". Small-scale experiments were used to study the effects of obstacles in turbulent deflagration. Models were developed and implemented in a CFD code to simulate the experiments performed. The first calculations performed show good agreement with experimental results. The experimental and analytical work in this area should continue. Furthermore, the analytical models developed should also be used in large scale experiments and ultimately should be used in full-scale containments.

The purpose of the paper "Combustion of Non-Uniform Hydrogen-Air Mixtures" by Mr. D.R. Whitehouse from AECL Whiteshell Laboratories, Canada, was to address the combustion behavior of stratified mixtures versus uniform mixtures, and vented versus constant volume of

hydrogen-air mixtures. Experimental results were presented with various levels of stratification at constant volume as well as vented volume cases. The results are of particular interest for pressure load calculations for severe accidents. The results from these experiments suggest that the effects of stratification on combustion pressure are similar to the effects of turbulence. Experimental work needs to continue to include additional parameters in the study (e.g., vary the location of ignition point).

The presentation on "Structure of Horizontal Hydrogen-Steam Diffusion Flames" by Dr. C.K. Chan from AECL Whiteshell Laboratories, Canada, focused on a systematic study on the stability and structure of various horizontal hydrogen-steam diffusion flames in experiments performed at AECL Whiteshell Laboratories. The stability and structure of diffusion flames play a significant role in the severe accident analysis. Thermal loads from stable diffusion flames directed towards safety equipment or the containment steel shell could pose significant threat to the equipment functionality and containment integrity. The study suggests protection from diffusion flames thermal loads using shielding with sheet metal. Future work in the area needs to address diffusion flame stability in a receiver vessel with conditions more prototypic to severe accidents (e.g., at high temperature, with steam, with pre-existing hydrogen, etc.)

Results from simulations of hydrogen deflagration tests using the discrete lumped-parameter burn model of the computer code GOTHIC were shown in the presentation titled "Modelling of Hydrogen Deflagration in Vessels Using GOTHIC" by L.L. Wang from Ontario Hydro Nuclear, Canada. The GOTHIC version 4.1 was used which has incorporated the burn models from HECTR for modelling the transportation and combustion of combustible gases during postulated accidents in LWRs. The study concludes that in all simulation cases, the analytical results for closed or vented volumes, with and without obstacles, produced conservative estimations of peak pressures when compared with experimental results. Future work for this code should concentrate on validation work using additional experiments and further code development should focus on improvements for the code to provide simulation results closer to experiments.

SESSION VI: NEW DESIGNS

Chairman: B. De Boeck, AVN, Belgium

In this session, four papers were presented that covered activities related to hydrogen mitigation for future nuclear power plants.

The first AECL presentation "Containment Air Circulation for Optimal Hydrogen Recombination", describes the hydrogen mitigation design provisions for a passive CANDU. The first-line defense for hydrogen mitigation is to design for the hydrogen to be rapidly mixed with the containment atmosphere and diluted to below flammability limits. Then, as hydrogen continues to be produced in the longer term, PARs can be used to remove hydrogen. The paper explains how this could be achieved in the design of a passive CANDU. A specific, buoyancy-driven overall circulation of the containment atmosphere is provided to passively cool the

containment and promote hydrogen mixing. PARs can be located within the recirculation flow, immediately downstream of the hydrogen source. This precludes the possibility that high hydrogen concentrations may exist far away from the PARs. This concept could be applied not only to CANDUs but also to PWRs.

The second AECL presentation "CANDU 9 Design for Hydrogen in Containment", describes the hydrogen mitigation design provisions for the CANDU 9. CANDU 9 is an evolutionary design developed by AECL. The paper discusses the layout and features of the containment which facilitate the distribution and mixing of any hydrogen, as well as the hydrogen control system which reduces local and global concentrations. Following a LOCA without emergency core cooling, at most 300 kg of hydrogen is expected to be released to the containment. The highest local concentrations occur in the accident room (fuelling machine vault) and the two adjoining rooms (steam generator enclosure and reactor vault). Without crediting hydrogen igniters or recombiners, the highest local concentration is less than 7% by volume. Future, more detailed, analyses will show that the hydrogen control system effectively limits both the local and global concentrations.

The EPRI presentation "PARs for Combustible Gas Control in Advanced Light Water Reactors", discusses the progress being made in the US to introduce PAR technology in both future ALWRs and certain US operating nuclear plants. In a non-inerted ALWR containment, two approaches, each employing a combination of PARs and igniters, are being considered to control hydrogen in design basis and severe accidents. In pre-inerted ALWRs, PARs alone control radiolytic oxygen produced in either accident type. The paper also discusses regulatory feedback regarding these combustible gas control approaches and describes a test program being conducted by EPRI and EDF to supplement the existing PAR test database. Preliminary findings from the EPRI/EDF PAR model test program are included. Successful completion of this test program and confirmatory tests being sponsored by the USNRC are expected to pave the way for use of PARs in ALWRs and operating plants.

The Italian presentation "Calculations Concerning the Capability of Passive Recombiners to Control Hydrogen Concentration in the Containment of an Advanced PWR", describes analytical work done in support of the hydrogen mitigation strategy of the AP600. The University of Pisa has developed a computer code, HOCRA, which is able to make an initial evaluation of the capability of catalytic recombiners to remove hydrogen from the atmosphere of the containment in accident conditions. The paper describes the physical and mathematical model of the code and reports calculations made for the AP600 in cooperation with ENEL. These first attempt calculations were carried out to verify the feasibility of using a system of passive recombiners to control the hydrogen. The results were encouraging, though also igniters seem to be needed to control the hydrogen in the In-Containment Refuelling Water Storage Tank (IRWST).

In summary, it was shown at the meeting that for new plants, the hydrogen issue can be taken into account at the design stage. This gives more freedom concerning the choice of the mitigation strategy. In particular, mixing can be enhanced by selecting a layout promoting natural circulation paths. This natural circulation can also be used to provide passive cooling of the containment, as was shown during the meeting.

Therefore, for future plants, a better solution to the hydrogen problem can be provided. This should help to meet the more stringent requirements put on future plants concerning the protection against severe accidents.

For new designs as for present plants, PARs appear as an essential component of hydrogen mitigation, not only for severe accidents, but also for design basis accidents. Experimental programmes are underway to confirm their performances for various conditions.

Depending on plant specificity's, and on the degree of assurance that the selected strategy covers all accident sequences to be addressed in the design, PARs can be combined with additional measures, like pre-inerting (BWRs) or igniters (to catch fast transients resulting in local high hydrogen concentrations).

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INTRODUCTORY SESSION

Chairman: G.W. Koroll (AECL, Canada)

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A DESIGNER'S VIEW OF HYDROGEN MITIGATION (Invited Paper)

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CA0000147

Introduction

It is commonplace today to design nuclear power plants to control and manage the potential effects of hydrogen evolution following accidents. Hydrogen management has two purposes -- to protect the containment structure itself against structural failure following rapid deflagration of hydrogen, and to protect systems inside containment against local damage due to hydrogen combustion.

Post-accident hydrogen control has become one of the normal elements of the design basis for containment and, through environmental requirements for internal components, on several NSSS systems. The term "design basis" is used here in the sense of inclusion in the design requirements of any system. National practices vary but, from a designer's point of view, any event for which processes or equipment must be provided to meet specific performance requirements becomes part of the design basis of the plant, even if the postulated event is not formally a "design basis accident".

"Normal" accidents, that is, those following which it is assumed that the safety-related mitigation systems respond according to design specifications, are relatively simple because peak temperatures are quite low, zirconium-water reactions are limited, and slow hydrogen production by radiolysis in the long term poses a straightforward design challenge.

"Severe" accidents are a different matter, in that it is generally true that one or more of the safety-related systems is assumed not to meet its performance specifications. Under such conditions, two problems arise. First, the quantity of hydrogen which must be controlled is uncertain due to the many possible event sequence pathways. Second, the physical location of hydrogen to be controlled is uncertain due to the complex evolution of many of these hypothetical accidents.

The containment design basis of most LWR plants includes the assumption of loss of coolant plus failure of the emergency core cooling system. This is a "severe" accident. In a PHWR plant the same assumptions would result in extensive damage to fuel, but the cool moderator water surrounding the fuel channels would prevent fuel melting and lead

to a "coolable geometry"; that is, the core structural integrity would be maintained. The design requirements placed on containment are, therefore, very different for these two reactor types. If it were assumed that the PHWR accident included (in addition) failure of moderator cooling, then slow core collapse would ensue and some fuel and fuel sheath melting would occur. Nonetheless, the shield tank water would prevent extensive fuel melting for many hours. Hydrogen evolution would be much less in both cases than one must expect following an LWR core-melt sequence.

The purpose of the above comments is to illustrate the other difficulty of hydrogen control design -- the fact that the probability of evolution of large quantities of hydrogen is very uncertain. This fact often is hidden by regulatory requirements specifying the "terms and conditions" for the design of hydrogen control systems; effectively, regulatory input to the systems design basis. Provided the specified conditions are manageable, design can proceed. The weakness of this approach is that the specified quantities may be changed later without notice, possibly nullifying the design specifications. It is better for the designer to select very conservative specifications which will be easier to defend later on.

Obviously, the design requirements of hydrogen control systems depend on the location of the release into containment, the timing of the release, and the quantity released. The systems appropriate for control may depend on these factors --a system designed to cater for control in the short term may become unreliable in the long term after dispersion and dilution take place and when power supply reliability may become questionable.

Overall Safety Goals

The basic design issue arises from the question of recurrence frequency versus magnitude of hydrogen production. This question is mainly concerned with whether hydrogen mitigation equipment should be installed at all. Those with high confidence in the reliability and performance of safety protective systems see this equipment as just another expensive addition to an already-adequate set of safety systems. On the other hand, those whose view of containment includes (either implicitly or explicitly) a requirement for assurance that it will "never fail", see hydrogen mitigation as an essential part of that assurance.

The evolving structure of water-reactor safety logic, at the high consequence end of the accident spectrum, rests on two main arguments: reliable prevention of severe core damage and maintaining integrity of the containment boundary. The first argument ranges from operational transients to serious accidents involving failure of one or more mitigation systems. However, beyond the limit of this prevention envelope (normally, beyond the "design basis" of mitigating systems) one would expect to find a variety of conditions which could challenge the integrity of the containment envelope. Hydrogen evolution is one of these

There is a new approach to "Reactor Safety in the Future" which is intended to provide full assurance to people that they will never be faced with a major disaster initiated by a

power reactor accident. This assurance usually is framed in terms of "no need for evacuation" or for "no long-term evacuation". The approach makes no reference to the probability of occurrence of this accident. It means that the containment must not fail catastrophically. It means that hydrogen mitigation is essential.

Hydrogen Management Systems Design Issues

Having once accepted the definite requirement for hydrogen management and mitigation, how does the designer then proceed to design these systems?

The most difficult aspect of hydrogen mitigation design, from the designer's point of view, is the uncertainty as to whether recombination, especially if done via ignition, will improve or degrade systems' safety. This uncertainty generates several questions of release location, timing, reliability, chemistry, location of ignitors and/or recombiners as well as the question of their proper location in relation to sources and atmosphere circulation patterns. In general terms these uncertainties create a "fuzzy" design problem, the worst kind of problem given the atmosphere of rigid regulation in which we find ourselves.

The overall issue is, of course, how to provide the high assurance of effectiveness of hydrogen management which is demanded by regulatory agencies? I understand that this issue will be one of the main concerns of this workshop. Its main components are availability, safety orientation, and function.

Availability is generally a matter of equipment qualification for service. Can the mitigation device (ignitor, recombiner, inert gas distribution system) be relied upon to operate as intended, when needed? For example, the electrical cables for AC ignitors are qualified for LOCA, the batteries for battery-powered ignitors have a full charge, the catalysts in catalytic recombiners are not fouled or poisoned, etc. Availability must be testable in-service, in one way or the other.

With regard to safety orientation, the device or hydrogen management measure should operate in a way that consistently improves safety in foreseeable scenarios. Safety orientation often is used in criticism of ignitors, where for example, a transient cloud of hydrogen is ignited and causes damage to essential equipment when it would, if left unignited, dissipate without incident. The issues of consequence of combustion, the prediction of pressure development, standing flames, venting effects, transition to detonation, etc. comes down to the question of safety orientation and how well we understand or can predict the outcomes of what we initiate. And is initiation preferable to doing nothing?

The device, when operating, must perform the intended function. That is, ignitors must be in the right places to intercept hydrogen and actually create benign deflagrations, removing hydrogen and distributing energy release over time and space. Or recombiners actually will remove hydrogen passively at the rates for which they were sized. The basis for function and performance claims is necessarily small-scale (relative to actual plant)

experiments or computer simulations. Extrapolation to full-scale plant configurations introduces further uncertainties.

Another approach to design is to attempt to control uncertainty by positive action, such as by maintaining assured forced or free circulation in the containment atmosphere.

Background of Hydrogen Mitigation Design in Canada

Since the initiating sequence for "worst-case" hydrogen production in the CANDU reactor is included in the design basis of the plant (LOCA+Loss of Emergency Coolant Injection), designers have worked on this problem for many years. Analysis and supporting research on accident scenarios involving fast production of hydrogen has been underway for more than twenty years. Due to the configuration of this design, the quantities of hydrogen produced under these conditions are limited; nonetheless, extreme cases involving core melt and disassembly have been considered.

The "spark" for installing hydrogen management and mitigation systems in CANDU plants was the hydrogen fire which occurred following the Three Mile Island accident. AECL, Ontario Hydro, Hydro Quebec, and NB Power were, at that time, in the midst of a large construction program involving ten units in Canada and several outside Canada, with four more domestic units planned. These circumstances gave a powerful impetus to the AECL hydrogen R&D program, already underway at the time (the Containment Test Facility, CTF, was under construction in 1979), as well as to the search for suitable designs for hydrogen management.

Reference 1 outlines the design approaches taken in various CANDU designs to date. Generally, the early single-unit stations rely on forced- or free-convective mixing for dilution, while the multi-unit stations operated by Ontario Hydro include distributed ignition systems employing glow plug ignitors. The difference in approaches taken in the two designs is related to the different mixing lengths between the release region (vault) and the balance of the containment volume.

Darlington, the latest CANDU station to be licensed in Canada, utilizes 54 glow-plug ignitors in each of the four interconnected reactor vaults. The three single-unit CANDU 6 units under construction in Korea utilize 44 glow plug ignitors per unit. The proposed CANDU 6 units in China will be the same. For the larger CANDU 9 units to be proposed in Korea, both ignitors and passive recombiners will be utilized. The latter uses a catalytic multiplate recombiner; several of these units will be distributed around the containment, particularly in zones of strong free convective flow.

A simplified sketch of the CANDU 9 containment is shown in Figure 1. In normal operation ventilation is passed through the reactor vault area, driven by ducted air coolers. Under post-accident conditions, air is drawn from the accessible area through the hydrogen mixing duct, passed through the ducted local air coolers and into the vault areas,

then discharged through the blowout panel area. The steam and hydrogen mixture is blown into the large accessible area, where the unducted air coolers high in the building also provide cooling and mixing. Free convection flow also is provided. The recombiner units are essentially boxes open on the top and bottom and which contain an array of metal plates coated with wetproof catalyst.

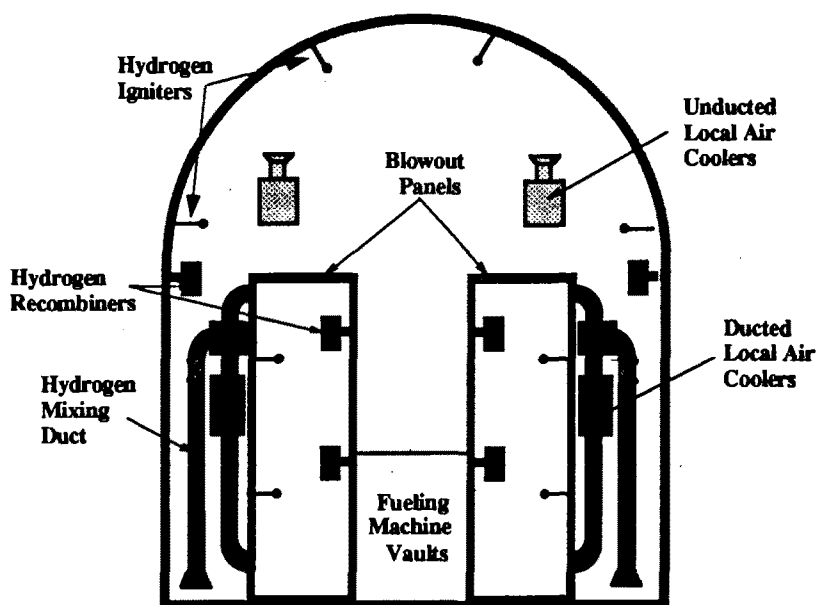


FIGURE 1 -- CANDU 9 Hydrogen Mitigation System

Future Safety Targets

One goal proposed for future nuclear power plant designs is that there be no requirement for emergency evacuation of the surrounding population or for permanent relocation, independent of the probability of occurrence of the accident. This goal generates very stringent requirements on containment design, including those related to effective hydrogen management. Much work is already completed in this area. Several matters pertaining to hydrogen management remain open; these issues will be discussed during the Workshop, with the aim of closing this subject from the point of view of reactor designers.

Summary

The basic problems remaining appear to be uncertainty, level of design assurance, and cost. This meeting is about finding simple and elegant solutions to post-accident hydrogen management. Better modeling, for example, may reduce uncertainty in scenario analysis, mixing, consequence prediction and better guide the placement of devices.

The well-established principle of defense-in-depth can be applied. Independent, diverse, and redundant measures for hydrogen management might be applied, for example, as follows:

1. Prevent flammable mixtures in containment. This can be approached through dilution by natural or engineered means, or by removal using catalytic recombiners, or by inerting. These methods may be complementary since recombiners supplement mixing while adding a mode of removal.
2. When prevention of flammable mixtures fails, the priority becomes prevention of violent modes of combustion, best achieved by preventing further penetration of the flammable range before ignition occurs; that is, by deliberate ignition. Mixing and removal both work to make eventual combustion more benign.

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CANADIAN PROGRAMME OVERVIEW



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ABSTRACT

In a severe accident, hydrogen is released into containment. When it is well mixed, the hydrogen mixture is non-flammable because there is sufficient dilution by the large containment volume. This is the desired end point. However, the release may occur into smaller compartments of containment, stratification may occur, and local pockets of flammable mixtures may arise if hydrogen is released faster than processes that mix and disperse it. Long term hydrogen generation from water radiolysis must also be considered. Hydrogen mitigation and control strategies adopted or considered in Canada include fan-forced mixing, glow plug igniters, recombiners, venting through filters, or combinations of these. The Canadian hydrogen programme is focussed on understanding hydrogen combustion behaviour and providing the data needed to demonstrate the adequacy of hydrogen mitigation and control strategies. The programme includes both experimental and modelling components of hydrogen combustion and distribution. Experiments include mixing tests, deflagration tests, diffusion flames, transition from deflagration to detonation, and testing the performance of igniters and recombiners. Modelling is focussing on the GOTHIC code as an industry standard. Detailed three dimensional modelling of gas mixing and combustion are underway, and a code validation matrix is being assembled for validation exercises. Significant progress has been made, highlights from which are being presented at this workshop.

INTRODUCTION

A severe accident in a CANDU™ reactor, such as a LOCA with failure of the emergency core cooling system, would lead to a release of hydrogen into containment. Ultimately, when the hydrogen is well mixed, the large volume of containment ensures that concentrations are below the flammability limit. However, local flammable mixtures may

arise during the period when hydrogen is being released, particularly during a rapid release from major piping located in smaller compartments of containment. Furthermore, in the months that follow an accident, a slow evolution of hydrogen generated by radiolysis of water needs to be considered. Therefore, hydrogen counter-measures are needed.

This paper describes the hydrogen mitigation and control strategies adopted or considered in Canada, and provides a review of the current focus of hydrogen research and modelling studies. Some programme highlights are provided.

BACKGROUND

Severe accident sequences evaluated in Canada, such as a LOCA with failure of the emergency core cooling system, release hydrogen into containment. The CANDU reactor is not immune to such sequences, although the amount of hydrogen generated by fuel sheath oxidation is less than that in a LWR accident sequence because the CANDU's moderator provides an additional heat sink to preserve a coolable geometry in the core. This, combined with the large volume of a CANDU containment ensures that hydrogen concentrations are below the flammability limit. This assumes that the hydrogen is well mixed. Flammable mixtures can arise during the period when hydrogen is being released, particularly if the release occurs rapidly from major piping located in a smaller compartment of containment. In the months that follow an accident, a slow but long term source of hydrogen is generated by radiolysis of water. Since local and temporary flammable mixtures may exist, and since a long term evolution of hydrogen needs to be managed, hydrogen mitigation and control is needed in CANDU containments. Strategies adopted or considered in Canada, which vary between single unit and multi-unit containment designs, include forced mixing by fans, deliberate ignition using glow plug igniters, passive catalytic recombiners, venting containment through emergency filters, or combinations of these [1,2].

The Canadian programme provides the necessary data and analytical tools needed to demonstrate the adequacy of these strategies. Early R&D focussed on understanding hydrogen combustion behaviour and igniter performance. Based on these studies, Ontario Hydro Nuclear installed glow plug igniters in its multi-unit containments, using engineering judgement to select strategic locations for them. Hydro-Quebec and New Brunswick Power ensured the availability of large natural circulation pathways and qualified local air cooler fans for their single unit stations. All Canadian utilities have relied on fan-forced mixing to disperse and dilute the hydrogen, taking advantage of the large volume of a CANDU containment. Some more recent studies using detailed three dimensional modelling of gas mixing, show that natural mixing process are sufficient to prevent damaging burns in multi-unit containment systems [3]. To manage long term evolution of hydrogen from radiolytic sources, both Hydro-Quebec and New Brunswick Power are evaluating the need for recombiners for their single unit containments whereas Ontario Hydro Nuclear has an Emergency Filtered Air Discharge System to vent hydrogen from its multi-unit containments.

One unique feature of Ontario Hydro Nuclear's multi-unit stations is the vacuum building into which steam and air are vented during a LOCA. The result is a local containment atmosphere that is not only subatmospheric, but also inert with steam while hydrogen is released. Such a mixture may become flammable from the oxygen-limited side of the ignition limits when the air backfills condensing steam. This is in contrast to single unit containments which are pressurised during a LOCA while air remains inside; as steam condenses, if such mixtures become flammable, it would be on the hydrogen-limited side of the ignition limits. Most of the data for glow plug ignition is near the hydrogen-limited side of the ignition limits.

To demonstrate the adequacy of these hydrogen mitigation and control strategies, detailed models are needed to show that the installed measures prevent damaging burns in containment. Furthermore, some issues need to be resolved, namely, the possibility and impact of turbulence induced flame acceleration, local detonations and standing flames. The Canadian programme provides the necessary data and analytical tools needed to demonstrate the adequacy of these strategies and to resolve the hydrogen issues.

HYDROGEN RESEARCH IN CANADA

The Canadian research programme on hydrogen behaviour is funded primarily by members of the CANDU Owners Group, namely, AECL, OHN, H-Q and NBP. Work is carried out at AECL Research at Whiteshell Laboratories, with an annual budget of about \$2.5 million. This is complemented by information exchange agreements. The goal of the programme is to obtain the information and tools to support the resolution of both licensing analysis and design concerns related to post-accident hydrogen behaviour. The four main issues are: deflagration; diffusion ("standing") flames; transition from deflagration to detonation; and, hydrogen mitigation.

Deflagration

This work develops and confirms a strategy of deliberate ignition, adopted by Ontario Hydro Nuclear, and contributes to the understanding of deflagration behaviour and development of an advanced deflagration model. There are five topics: Flame Acceleration; Simultaneous Flames; Concentration Gradients; Vented Combustion with Obstacles and Fan Turbulence; and, Interconnected Volumes.

Flame Acceleration affects the peak pressure in a burn and arises due to gas expansion and interaction with obstacles, inducing turbulence which increases the flame speed. New data suggests that there is a limit to this effect, since as turbulence increases, there exists a critical turbulent fluctuation velocity above which the flame can not propagate.

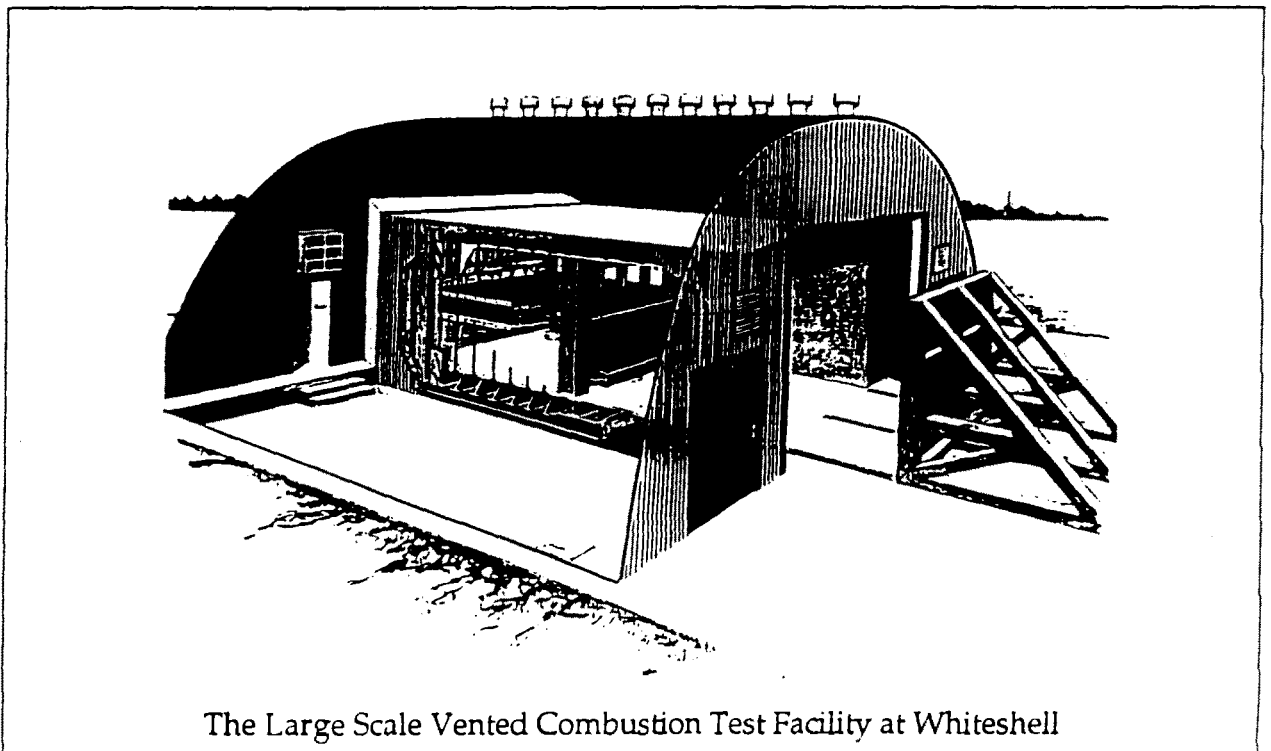
Simultaneous Flames are possible when multiple igniters are used, and there is concern that this could result in reduced venting effectiveness. Testing to date has shown that this will not adversely affect peak pressure.

Concentration Gradients are possible when hydrogen is released near the top of a compartment or room. Since deliberate ignition strategies require adequate mixing, so that ignition occurs along the "assured ignition limit" compositions, preliminary tests in a non-vented vessel with a concentration gradient were performed which generally show that peak pressures are less than if the same quantities are well mixed. The exception is when the equivalent well-mixed concentration is lean or nonflammable; incomplete combustion also affects the comparison. A paper on this work is presented at this workshop [4].

Vented Combustion with Obstacles and Fan Turbulence is a concern, because the induced turbulence increases the burning rate, promoting more complete combustion and therefore higher a peak pressure. Tests with 10% H₂ showed an increase in peak pressure, but no increase if the H₂ concentration is 12 - 16%.

Combustion in Interconnected Volumes remains a concern for modelling, and tests are planned.

A major milestone of the deflagration programme is the completion, commissioning and operation of the new Large Scale Vented Combustion Test Facility. A paper presented at this workshop describes this in detail, and show some early test results [5].



Diffusion "Standing" Flames

This work determines the stability limits for a H₂-Steam diffusion flames in air-steam atmospheres, and the temperature field of and the heat transfer from non-circular and non-vertical H₂ diffusion flames. A paper on this work is presented at this workshop [6].

Transition from Deflagration to Detonation

This work determines the criteria for transition from deflagration to detonation ("DDT") in terms of mixture sensitivity, turbulence, shock strength, and geometry of the enclosure. A model to predict DDT is under development. It has been shown that DDT is always preceded by a local explosion. This can be caused by shock focusing (local strengthening of the leading shock by flame acceleration) or by turbulent burning (local mixing of hot burnt gas with unburnt mixture following flame quenching).

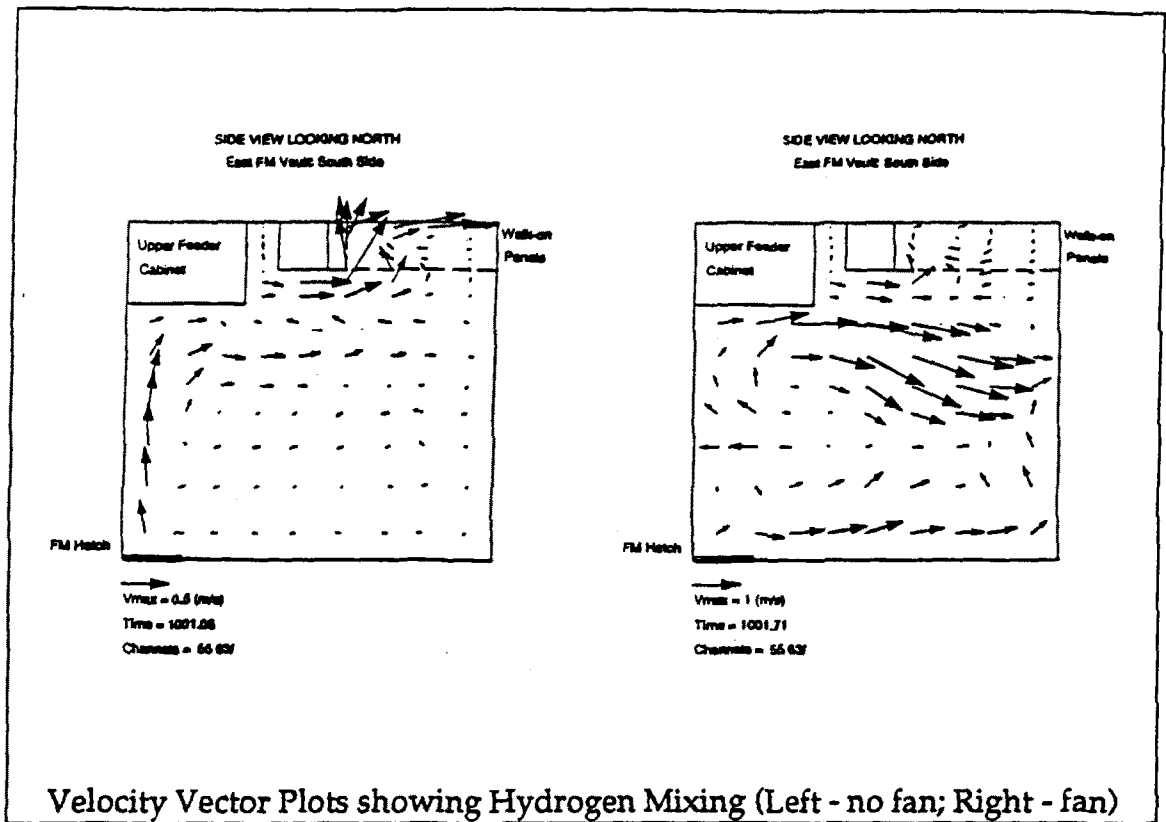
Hydrogen Mitigation

Current work is to determine the pre-engineering performance characteristics of the AECL wet-proof catalyst for application in post-accident removal of hydrogen in containment. An auto-catalytic recombiner has been developed which uses the enthalpy of reaction to induce natural convective flow over the catalyst elements. Recently, a catalyst formulation has been designed and tested which enables operation in high hydrogen concentrations without the corresponding high temperatures. A paper on this work is presented at this workshop [7].

HYDROGEN MODELLING IN CANADA

Canada is adopting the GOTHIC computer code [8] as the industry standard tool for detailed three dimensional modelling of gas mixing and hydrogen combustion in containment. The ability to perform such detailed modelling is necessary to demonstrate the adequacy of installed hydrogen mitigation and control measures. The models must be capable of calculating gas mixing patterns in containment, and evaluating the effects of local burns in non-uniform mixtures. A recent analysis applied to a multi-unit containment shows that local temporary mixtures may be flammable before the gases disperse to the rest of containment. For such cases, local burn models provide a better understanding of the adequacy of mitigation measures to prevent damaging burns. Some highlights of the analytical programme include:

- Detailed three dimensional modelling of the gas mixing patterns in a vault of a CANDU containment were performed using the GOTHIC code to demonstrate the adequacy of the hydrogen mitigation measures adopted by Ontario Hydro Nuclear. A paper on this work is presented at this workshop [3].



- The "lumped parameter" burn model in GOTHIC was used to simulate combustion behaviour in intermediate and large scale tests, including the effects of venting and obstacles, with reasonable results. A paper on this work is also presented at this workshop [9].
- The full three dimensional features of GOTHIC were used to simulate vented combustion in the intermediate scale Containment Test Facility and the Large Scale Vented Combustion Test Facility, also with reasonable results, with additional information to assess the influence of vent flows and buoyancy on flame propagation.

In addition to these modelling activities, the Canadian Industry has organised a Code Validation Team to assemble the data needed to validate codes, including GOTHIC. Work is in progress.

SUMMARY

Hydrogen mitigation and control strategies are needed for CANDU containments. Those strategies adopted or considered have been described, and some design differences between single and multi-unit containments have been discussed. The Canadian hydrogen programme is focussed on understanding hydrogen combustion and distribution, and in providing the analytical tools to carry out detailed modelling. The programme was described and recent highlights were presented. Hydrogen research is addressing issues and design concerns related deflagration, standing flames, transition to detonation and mitigation. Hydrogen modelling is focussed on a standard three dimensional gas mixing and combustion code, using GOTHIC as the industry standard.

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SESSION I

ACCIDENT MANAGEMENT AND ANALYSES

Chairman: J. Duco (IPSN, France)

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1.1 HYDROGEN MITIGATION SYSTEMS— A CANADIAN REGULATORY PERSPECTIVE

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Abstract:

This is a discussion paper to examine the regulatory requirements that may be necessary for the design, operation and maintenance of the hydrogen mitigation systems. These systems (if deemed necessary to maintain the containment function), may be considered to be a part of the containment systems. Therefore, these requirements are derived mostly from the AECS Regulatory Document R-7, which specifies the requirements for containment systems for CANDU nuclear power plants. Some additional requirements, which are specific to these systems have also been included. These requirements relate to a systematic examination of the hazards of hydrogen, the design basis for the mitigation systems, their functional and design requirements, analytical support to justify their selection, and operating and testing requirements. The requirements for severe accident have not yet been developed. It is, however, anticipated that the design of the hydrogen mitigation system would be such that future requirement can be accommodated.

These requirements are intended for application to the new reactors in Canada. For the existing reactors, their application will be subjected to practicability.

1.0 Introduction:

In the last 10 -15 years, there have been significant advances in our understanding of the behaviour of hydrogen inside the containment, in the analytical as well as experimental area. Therefore, as compared to the ad-hoc design decisions of a decade ago, these developments now permit a more systematic approach. It was felt that there is a need for developing the regulatory requirements for the design, operation and maintenance of these systems. This discussion paper is a first attempt towards fulfilling this need, and is a part of an early phase of the consultation process with the Canadian nuclear community.

2.0 Hydrogen Mitigation Systems:

For certain postulated accidents there is a potential for fuel heat up upon loss of cooling which may result in the release of a significant amount of hydrogen inside the containment. The hydrogen may also be released in the later part of the accident due to radiolysis of the sump water by the fission products. At some time during the accident, the hydrogen may form a combustible mixture with the air and a random spark may ignite it. The hydrogen burn, under certain conditions, may generate high pressure or high temperature fields and has the potential to compromise the performance of the containment required under the accident conditions. The purpose of the hydrogen mitigation system is to ensure that the hydrogen is dispersed, inerted or combined with oxygen in a controlled manner so that the consequences of a hydrogen burn are benign.

Table 1 shows the mitigation equipment installed in the Canadian reactors at the present time. They are centrally powered coil igniters. A re-evaluation is in progress and the following are under consideration .

1. Centrally powered coil igniters and/or self powered spark igniters for generating a burn at the earliest possible moment.
2. Catalytic recombiners for keeping the hydrogen concentration low by recombination reaction; it may also generate buoyancy induced mixing and thus further dilution of the hydrogen concentration.

Air cooling units may or may not be a part of the hydrogen mitigation system. These units are normally operating and therefore, will attempt to mix the released hydrogen and steam into the rest of the containment, if they remain functional or as long as they can be kept functional (depending upon the availability of power and environmental qualifications) in the post accident phase.

Inerting the containment atmosphere in a local area or in the entire containment volume (though currently not under consideration in Canada) is also a potential mitigation measure.

3.0 Systematic Examination of the Hydrogen Hazard:

A systematic examination of the hazards posed by the presence of hydrogen in each sub-volume inside the containment, should be done. The hydrogen may be present in a sub-volume either due to the presence of the source or may be transported from other sub-volumes. This is generally a qualitative assessment (supported by some scoping analysis) to ensure that all aspects of the hazard have been taken into account for each sub-volume and to determine which mitigation measures would be suitable.

This assessment should evaluate in each sub-volume, the potential for a hydrogen burn and

the consequent threat to the

- containment boundary,
- critical internal structures whose failure may render the analysis speculative,
- instrumentation and equipment which must be available for the post accident management of the plant,

The vulnerability of the critical areas stems from a global or a local hydrogen burn which may result in a high pressure pulse or a high pressure differential across certain enclosures. Also a flame flash back to the source of hydrogen may create high temperature fields in the near vicinity.

The magnitude of the threat will depend upon the severity of the burn which in turn is a function of the sensitivity of the hydrogen mixture at various locations and times. The mixture sensitivity in a given sub-volume will depend upon the location, rate and the amount of hydrogen released, the plant configuration, and the effectiveness of the mechanisms involved in its transport to the other parts of the containment.

The amount of hydrogen released may be small or large depending upon the accident sequence and the ultimate heat sink for the fuel. In the longer term, the radiolysis of the water in containment sump by the fission products may result in a steady release of hydrogen, though at a rate slower than the initial phase of the accident. The changes in the concentration of steam in a given sub-volume would also have a significant effect on the mixture sensitivity. The hazard assessment should also take into account other factors which may enhance the severity of the burn such as non-uniform concentrations, the size of the opening between sub-volumes, the presence of obstacles in the flame path.

4.0 Design Basis Events for the Mitigation Systems:

The design basis for the hydrogen mitigation measures are a subset of accidents given in the Consultative Document C-6 [1], which result in a significant release of hydrogen inside the containment. These accidents, listed in Table 2, involve a loss of coolant to the fuel or a loss of heat sink. As is the regulatory practice in Canada, the design basis also include event combinations involving additional failures such as a failure of a safety system (e.g. emergency core cooling) or mitigating process systems (e.g. class IV power or back up heat sinks). Other failures which may occur in the long term phase of the accident (e.g. earthquake) are also to be considered. These event combinations are listed in Table 3.

Consideration should also be given to any possible consequential effects which may contribute to additional release of hydrogen. In CANDU reactors, the moderator is credited as an ultimate heat sink for the fuel. If this heat sink is lost as a consequence of a design basis event combination, then additional hydrogen released into the containment should also be taken into account. For example, some recent tests have shown that a failure of a pressure tube has the potential for a loss of moderator inventory due to the ejection of the end-fittings.

A partial loss of moderator inventory can also occur in the case of a loss of coolant (LOCA) inside the calandria and a loss of emergency core cooling (ECC), if there is a significant entrainment of the moderator inventory by the steam via the rupture discs. If these possibilities are confirmed by further tests or analysis, then an in-core with loss of ECC and a partial/full loss of moderator inventory would form a design basis accident.

At present in Canada, the requirements for the hydrogen mitigation systems for severe accidents (resulting from an almost indefinite loss of heat sink for fuel and generation of a much larger amount of hydrogen) have not yet been fully developed. It is anticipated, however, that the selection and the design of the mitigating systems (presently under consideration) would be such that these future requirements can be accommodated.

5.0 Functional Requirements and Acceptance Criteria:

For all design basis accidents, the mitigating systems must assure with a high degree of confidence that the structural integrity of the containment boundary and its critical internal structures is maintained. They should also assure that a standing flame attached to a source of hydrogen, does not damage any component which is required for post accident management.

A conservative criterion to ensure that the functional requirements are met, is that hydrogen-air-steam mixture composition in all (judiciously chosen) containment sub-volumes always remains at or 'near' the deflagration limits. This is to ensure that if a hydrogen burn occurs, the flame speed remains low (in spite of any flame acceleration that might occur) and the consequences are such that there is no significant increase in containment leakage.

This criterion is somewhat more specific than that given in Regulatory Document R-7[2], where the requirement is to prevent explosion or deflagration. The term 'near the deflagration limit' (representing the acceptable deviation) is yet to be defined and it will depend upon the confidence (in the analytical methods and their experimental support) in predicting the consequences of a burn. It may not be possible to meet this criterion at all times for the sub-volume in which the source of hydrogen is located. A special assessment will be required for this location to demonstrate that the threat to the containment function is acceptable.

6.0 Design Requirements:

Since the hydrogen mitigation systems are a part of the containment sub-systems, they should meet all the relevant design requirements given in R-7. The most important design requirements that the hydrogen mitigation systems need to meet, throughout their mission time, are as follows:

1. The selection and the design of the mitigating systems should be such that their functional requirements are met for all design basis events, i.e. the number of igniters and/or recombiners and their location inside the containment is such that they are capable of maintaining the hydrogen concentration near the deflagration limits for all events at all times. This would also impose performance requirements on the igniters and the recombiners (and the air cooling units, if credited) to ensure that the hydrogen is mixed burnt or recombined at a rate which is consistent with the timing and the rate of hydrogen release in a given accident sequence. Detailed performance requirements for the individual igniter or recombiner (e.g. mixture composition to initiate a burn or recombination rate at various hydrogen concentrations) should be developed.
2. All components of the hydrogen mitigation systems need to be environmentally qualified taking into consideration the most severe pressure, temperature, humidity, radiation conditions that they may be subjected to, when required to perform their function. The vulnerability of the air cooling units (if credited) to the consequences of a hydrogen burn is of particular concern.
3. Since the mitigation systems may be required to function following a seismic event that may occur some time in the post accident phase, all the necessary components should be qualified to function for the design basis earthquake.
4. Monitoring, control and manual initiation of the mitigation systems should be possible from the main as well as the secondary control rooms.
5. The hydrogen mitigation systems being a part of the containment sub-systems should have a reliability which is consistent with that required for the containment, i.e. their reliability should be better than 10^{-3} per demand.
6. The design of the hydrogen mitigation systems should conform to the requirements set for the containment with respect to separation, redundancy and independence between the initiation logic channels. It should also satisfy the 'fail safe' criterion for various component where applicable, and no single component failure should result in loss of system function. There should also be sufficient component redundancy to satisfy the reliability requirements.
7. The support services for the hydrogen mitigation systems (e.g. instrument air, electric power, cooling water) should have sufficient redundancy and independency to conform to the requirements for containment systems.
8. A reliable indication of the mixture concentration in the critical areas inside the containment should be available to the operator to monitor the progress of the event sequence and the effectiveness of the mitigation measures.

7.0 Analysis Requirements:

The detailed design of the mitigating systems may be based on the systematic evaluation of the hazard posed by the presence of hydrogen inside the containment. Some engineering judgement and scoping analysis could be used in determining the location and the number of igniters and/or recombiners. This selection must be justified by a detailed analysis.

The consequence analyses should cover the following:

1. Scope of the analysis:

The scope of the analysis should cover all design basis event sequences derived from Tables 2 and 3 and should extend to the long term phase where a steady release of hydrogen may occur due to radiolysis. Consideration should also be given to the factors which may impose different demands on the hydrogen mitigating systems, at some time after the initial steady state is reached. Some examples of such factors are the possibility of delayed initiation of emergency core cooling, the break down of the emergency cooling system during its mission period, or a seismic event happening.

Events with a total frequency of occurrence greater than 10^{-7} per year that are not included in the design basis set, and result in a greater hazard, should be identified. Sensitivity analysis should be done to assure that the mitigation systems are adequate for these accidents.

2. Analysis Methodology

Assessment and scoping analysis should be done by considering all possible locations for hydrogen release to identify the limiting cases. In particular, pipe breaks at the header-to-header interconnect or at the interface boundaries between the heat transport system and other systems (e.g. emergency core cooling and shutdown cooling systems) would have different implications than for the rest of the heat transport system, because of their location. The pipe breaks inside the feeder cabinet and inside the calandria are other examples of situation which require special consideration. Similarly, various containment configurations (affecting pathways for the transport of the mixture from one sub-volume to another) also need to be considered to determine the limiting cases. Consideration should also be given to the timing of the release of hydrogen and steam.

Final analysis for the limiting cases should be done with well validated (lumped parameter and/or 3-D distributed parameter) codes using judiciously chosen sub-volumes (or nodes). This is to identify the bounding mixture compositions at all critical locations to demonstrate that they are at or near the deflagration limit, and that the pressures generated inside the containment do not threaten the containment boundary or any critical internal structure.

3. Sensitivity Studies

Sensitivity studies should be carried to optimize the design and develop operating strategies to minimise the potential adverse effects and maximize the beneficial effects.

For instance, while the fans from the air cooling units enhance mixing which results in the dilution of hydrogen concentration, the condensation of steam de-inertises the mixture. Thus a more sensitive combustible mixture may form in both within the air cooling units and some containment sub-volumes. On the other hand, recombination units may generate buoyancy induced flows which would enhance mixing.

4. Standing Flame

A hydrogen burn may result in a flame that may be attached to the source of hydrogen such as: break location or discharge of the air cooling units. The flame may also be generated by the igniters and recombiners. Where there is a potential for standing flame to occur and cause damage to some critical equipment or containment boundary, a conservative estimates of the steam and hydrogen concentration should be made in assessing the stability of the flame. The survivability of the equipment, instrumentation, cabling, or the containment boundary should be assessed based on the estimated heat fluxes generated in and above the flame. Sufficient experimental data base should be available to support the model used to estimate the heat fluxes.

8.0 Requirements for the Computer Codes used in the Analysis:

The analytical codes should conform to all the requirements of CSA N-286.7 for scientific computer codes for nuclear power plants. In particular, the following must be considered in qualifying the code for use.

1. The choice of the analysis code to simulate a given situation should be justified from the point of view of suitability for the scenario under consideration. For example, a lumped parameter code may be suitable in one case whereas 3-D distributed parameter or a combination of the two may be suitable for other situations.

A special attention may need to be paid to the choice of sub-volumes where there is a tendency for the hydrogen to accumulate and/or steam to condense especially, in the absence or partial impairment of forced mixing of the containment atmosphere by the air cooling units. Sub-volumes served by the discharge ducting of the air cooling units, the reactor building dome, the vault cooling system room (in Bruce type reactors) are examples of the cases where a more sensitive mixture is likely to develop.

2. A systematic examination should be done to verify that the code(s) to be used is

validated for all the important phenomena that are likely to occur in a given situation. Special consideration should be given to the flow regime, geometry and the scale effects.

3. Sufficient experimental information should be available to adequately support the various models that are used in the codes. The following are some examples where the relevant data base is necessary.
 1. Steam condensation models (in the presence of non condensible gases) for air cooling units, cold surfaces and sprays.
 2. Models for mixing (natural and forced convection, etc.) inside containment
 3. Models of the catalytic recombiner performance.
 4. Models for the combustion initiated by an igniter and its propagation into non-homogeneous mixtures.
 5. Models for flame acceleration in the presence of obstacles and through openings between sub-volumes.

9.0 Operating Requirements:

The hydrogen mitigation systems should satisfy the operating requirements for the containment subsystems given in R-7. Some important ones are listed below:

1. The status of the important sub-system components/equipments and the means of intervention, if necessary, should be made available to the operator, from both control rooms.
2. Adequate instrumentation should be provided to give the operators a clear indication of the hydrogen concentrations and temperature fields in critical areas within containment. This is to enable them to assess the threat posed to the containment envelope, critical internal structures and the equipment required for post accident management, from both control rooms.

10.0 Commissioning, In-Service Testing and Inspections Requirements:

To ensure that the necessary hydrogen mitigation systems meet their minimum performance requirements commissioning and in-service tests and inspections have to be carried out according to the specifications given in R-7. Some important requirements specific to the hydrogen mitigation systems are outlined below:

1. To the extent practical, in-situ commissioning and periodic tests should be carried out to verify that the hydrogen mitigation systems can carry out their functions, under the expected operating conditions. For instance, in situ-testing of the igniters to ascertain that they can reach their design steady state temperature under representative conditions of turbulence need to be carried out.
2. Where the in-situ tests can not be done for the representative conditions, tests in a laboratory may be carried out for the representative temperature, pressure, radiation fields, humidity, aerosols and sprays present under post accident conditions, to confirm that the igniters and/or catalytic recombiners can still meet their minimum performance requirements. Since the catalytic recombiners are particularly vulnerable to poisoning, all the chemical species that are likely to occur inside the containment in the short as well as long term post accident phase, and which affect the performance of the catalyst, should be included in the test program.
3. Testing of the mitigation system components and equipments are to be carried out at a suitable frequency to demonstrate compliance with their reliability and minimum performance requirements. Special attention must be paid to the parameters susceptible to ageing effects. The poisoning of the catalyst in the recombiners and the remaining battery life of the spark igniters are some relevant examples.

11.0 Discussion:

It is recognised that there may be a number of difficulties in meeting all these requirements. The analysis tools have not yet been fully developed and their validation will require considerable additional research work. The validation of the codes, which will take into account the geometry and scale effects, especially when the mixture composition deviates significantly from the deflagration limits, can be very difficult and expensive. It may be difficult to meet the reliability requirements especially for the air cooling units. Also, a break in the feeder cabinet is very difficult to assess because of the presence of hydrogen source, repeated obstacles and uncertain geometry as a consequence of the break.

While the analytical and experimental research effort is in progress to address these issues, the decisions with regards to the design (the number and locations of the igniter and/or recombiners) of the mitigation system will have to be made. There may also be practical consideration in locating these equipment in certain places e.g. the dome area. It is also worth noting that the recombiners, though currently look very promising, are in the early stage of their development. Their ageing effects are unknown as they have not been exposed to the containment atmosphere for any significant length of time.

Given all these uncertainties, it is clear that in making those decisions, a fair bit of engineering judgement will have to be used, and it should be based on the current state of knowledge and prudence.

12.0 Summary:

In this discussion paper, a brief overview of the requirements that may be necessary for the hydrogen mitigation systems has been presented. These requirements have been derived mostly from the AECB Regulatory Document R-7 for containment. Some additional ones which are specific to the hydrogen mitigation systems have been included. These requirements are intended for application to the new reactors in Canada. For the existing reactors, their application will be subjected to the practicability of retro-fitting.

Since in Canada, the regulatory requirements for the severe accidents have not yet been fully developed, it is anticipated that the design of the hydrogen mitigation system would be such that the future requirements can be accommodated.

13.0 REFERENCES:

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2. "Requirements for Containment systems for CANDU nuclear power plants", A regulatory Policy statement. AECB regulatory document R7, February 21, 1991.

Table 1 Mitigation Systems Currently Installed in Canadian Reactors

Facility	Mitigation System
Darlington	58 Tayco igniters per unit
Bruce A and Pickering A	12 Tayco igniters per unit
Bruce B and Pickering B	16 Tayco igniters per unit
Gentilly 2 and Point Lepreau	None (other than the air cooling units)

TABLE 2 Single Initiating Events

No	Design Basis Events
1	Failure of any piping or header in any fuel cooling system
2	In-core loss of coolant accident such as: flow blockage
3	Failure of any end fitting
4	Failure of a fuelling machine to replace a closure plug
5	Failure of any piping in the steam generator feedwater or steam systems
6	Failure of steam generator tubes

TABLE 3 Event Combinations

No	Design Basis Event combinations
1	Any of the single events 1 to 6 of Table 1 coincident with or followed by failure of a mitigating process system that must change state e.g. a loss of class IV with operator failure to initiate the local air coolers manually, loss of secondary side back-up heat sink.
2	Any of the single events 1 to 6 of Table 1 accompanied by or followed by a loss of emergency core cooling in the short or long term.
3	Any of the above event combinations followed by a common cause event (earthquake, fires, internal flooding, etc.) in the long term.

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1.2 RATIONALE FOR THE IMPLEMENTATION OF HYDROGEN MITIGATION TECHNIQUES ON FRENCH PWRs

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Abstract

The actual benefit, in terms of improved safety, of implementing specific counter-measures to deal with the issue of hydrogen in the containment of a given type of PWR, under severe accident conditions, should be assessed in as realistic a way as feasible before a decision is finalized. Existing methodologies, based on current codes and data bases, are expected to provide a first-degree assessment, with specific input data and models intended to compensate for physical uncertainties. The IPSN has launched a comprehensive research and assessment program on such key hydrogen behavior uncertainties. A state-of-the-art realistic assessment of hydrogen production has been recently prepared by the Institute. Regarding hydrogen evolution and distribution in the containment, priority is placed on sub-compartments, where high hydrogen concentrations could result in detonation, either directly or through a deflagration-to-detonation transition. In a first step, efforts are being directed towards setting criteria which delimit the non-transition realm for representative air-steam-hydrogen mixtures at high temperature; IPSN participation in the RUT experiments is expected to provide relevant data. As to a better assessment of the hydrogen distribution between sub-compartments, as well as to the pressure-time history onto the walls in the event of either a deflagration or a detonation, the new 3-D, meshed TONUS code is currently being developed at CEA for IPSN, partially based on models already existing in the TRIO-EF/VF, CASTEM 2000 and PLEXUS codes. Some highly important thermal-hydraulic models, like those for wall condensation and aspersion, will be validated by various analytical experiments and by specific tests under representative severe accident conditions in the projected IPSN TOSQAN facility at Saclay. The TONUS code should also provide guidance for an optimized positioning of hydrogen measuring devices and/or of hydrogen depleting devices like recombiners, should the decision be made to install them. As the utility has envisioned the possibility of installing recombiners on its PWRs of the P'4 and N4 series, the IPSN has simultaneously launched an experimental program to verify that some existing measuring devices and/or recombining systems can remain efficient under severe accident conditions, and do not cause adverse effects: the H2PAR facility at Cadarache should provide such representative environments, with fission product simulants, to complete their characterization.

1 The Hydrogen Risk on Current and Future PWRs

1.1 Identification of IPSN needs

Severe PWR accident studies have been carried out during past years in many countries, for improving risk prevention and mitigation, as well as accident management operational means; these studies indicated the possibility of an early containment failure resulting from

a rapid combustion of hydrogen released into the containment after the onset of core damage (containment « gamma » mode of failure according to WASH-1400). Qualitative IPSN assessments performed at the end of the 80's tend to prove that such a risk is highly improbable on French PWRs. Nevertheless, it appeared necessary to quantify more accurately the risks of such a gamma failure mode, so as to place the IPSN in a strong position to assess the safety files on current PWRs or future projects, and in particular to judge the interest of additional mitigation devices. So as to enhance its expertise capabilities on key hydrogen issues under representative PWR severe accident conditions, the IPSN has launched a dedicated R & D program, comprising both experimental research and modelling. This program has been deliberately set in the context of an extended partnership, first in France with the utility Electricité de France, but also abroad, with organizations involved in hydrogen research, such as FZK, GRS, the Kurchatov Institute, COG/AECL and NUPEC. Such a partnership, along with international expert seminars, like the present one organized by OECD/CSNI, is expected to promote a worldwide basis for the application of research and expertise work carried out everywhere.

As mentioned above, IPSN studies on hydrogen behavior aim at providing its safety analysts with adequate technical basis, so they can properly assess hydrogen risk in the safety files concerning current reactors and future projects; more specifically, the resulting improvement in knowledge should aid in the accomplishment of the following duties:

- a) the assessment of the risk of containment early failure by hydrogen combustion (« gamma » failure mode) on reactors currently in operation; the relevant probabilistic risk assessment is part of the level-2 probabilistic safety study on French 900MW(e) PWR units initiated by the IPSN in 1994;
- b) the assessment of the way this risk is addressed by the utility in its safety files; such an assessment is achieved in the framework of the overall safety analysis of the operating reactors for severe accidents; it is apparent that this task will involve extensive comparisons with the most recent trends in international practice based on updated knowledge, which implies regular contacts and exchanges with foreign organizations like GRS, AVN, USNRC or NUPEC and expert groups which advise the safety authorities;
- c) the assessment of technical proposals by the utility and/or the vendor to deal with the hydrogen risk on current and future reactors, in particular the European Pressurized Water Reactor (EPR);
- d) the proposal of directions for optimized PWR safety improvements in the field of hydrogen: an improved understanding of the physical phenomena should result in a better prioritization in the search for means to prevent or mitigate the hydrogen risk.

1.2 Assessment tools currently available at the IPSN.

The codes currently available for the assessment of the hydrogen risk are the following:

- a) « Point » codes, either single- or multiple-volume codes, embedded in large code systems, for the quantification of the hydrogen behavior during a PWR severe accident sequence: among these, the JERICO code of the system of codes, and other codes of the same type, such as RALOC, MELCOR,... available in the framework of international exchange agreements. These codes are not designed to provide a description of local phenomena or dynamic effects: their purpose is simply to calculate a fission product « source term » i.e. a fission product release, typical of a given category of accident scenarios and defined on the basis of global assumptions about the thermal-hydraulic behavior of the containment; such assumptions should in the future be supplied or validated by more mechanistic codes, which are coherent with available experiments.
- b) Multi-dimensional codes, specialized in the assessment of specific risks; among them: TRIO-EF and TRIO-VF for hydrogen distribution and stratification phenomena, PLEXUS for hydrogen detonation.

These codes feature a detailed description of the physical phenomena of concern; nevertheless, their validation requires large-scale experiments with extensive results at specific locations, which are not currently available experimentally with the required degree of detail; in addition, there is no fully appropriate coupling methodology for related phenomena, e.g. for accounting for the initial thermal conditions of the containment in the hydrogen distribution codes; lastly, there is currently no mechanistic model for accelerating combustion and/or turbulent flames in the codes available.

1.3 The physical phenomena concerned. Major uncertainties affecting the safety assessments.

The physical phenomena to be properly quantified for an appropriate assessment of the hydrogen risk under severe PWR accident conditions comprise hydrogen generation, its time-dependent evolution and distribution in the containment, hydrogen deflagration and its possible transition to a detonation, containment response to the various types of explosion and the behavior of instrumentation and important systems in accident management.

Regarding the hydrogen generation issue, zircaloy cladding oxidation by steam is the dominant source of hydrogen for severe arrested in-vessel accidents; the amount of hydrogen resulting from the total oxidation of the zircaloy inventory of the core is known; for partial core melts, there is a significant uncertainty over the amount of hydrogen produced due to the relatively poor knowledge of the core behavior during the late slumping stage: this results in uncertainties regarding the interacting surface area for the Zr-H₂O, hydrogen generating reaction once the initial geometry of the core has been lost. In-vessel oxidation of the other metals constituting the vessel internals, and of the corium depends on the temperatures reached locally; uncertainties also exist on the flammable ex-vessel gas generation rate during the so-called « high temperature » interaction stage between the corium and the basemat concrete, which is expected to last on the order of fifteen minutes, as an order of magnitude; nevertheless, the total amount of hydrogen generated after a given interaction time between corium and concrete is well known. Water radiolysis and metal oxidation in the containment are relatively negligible contributors in the short term, i.e. a couple of days, in the event of a severe accident. In conclusion, it is apparent that, for a correct assessment of the hydrogen risk, the proper evaluation of the amount of this gas generated during the in-vessel core melt stage is a major issue. Gaining additional knowledge on this matter is closely dependent on R & D activities concerning in-vessel core degradation. In the short term, it appears that the elaboration of a comprehensive state-of-the-art report on in-vessel phenomena should constitute an appropriate first response; such a report should review the lessons learned from the various past experiments and highlight typical values to be used for hydrogen generation. In a subsequent stage, the hydrogen generation rate could be another issue to be settled; it might affect the number and/or the design of recombiners, should this mitigating system be proposed.

Regarding the hydrogen and steam distributions in the containment, the issue posed by the French PWRs in operation is the risk of high transient hydrogen concentrations in some sub-compartments, possibly leading to a deflagration-to-detonation transition; the lumped parameter codes currently available lack the capacity to describe stratification phenomena or hydrogen distribution in multiple-compartment geometries, whereas multi-dimensional codes are inappropriate to conveniently describe all the thermal and geometric features of a PWR containment, which would require a time-consuming elaboration of the data set. As to the validation of both types of code, in view of their reliable application to the reactor case, no currently existing experiment appears to be sufficiently representative, nor has the extensive instrumentation required for such a task: reactor application problems comprise extrapolation capabilities as regards scale, timing of the accident scenario and the

complexity of initial boundary conditions on a reactor. If supplementary systems were to be proposed, to be effectively positioned in the containment for improved hydrogen risk management, such as on-line hydrogen measurement or depleting devices, qualified computational means to assess the time-dependent hydrogen concentration in the containment would be of prime value for the demonstration of the appropriateness of the lay-out. Regarding the assessment needs on currently operating reactors, the priority is to be placed on containment sub-compartments, where a local evolution of hydrogen could result in concentrations high enough to allow a transition to a detonation.

As concerns hydrogen deflagration, the relevant limits in ternary hydrogen-air-steam mixtures are well established and are not significantly sensitive to the geometry of the boundaries in the range of volumes of concern. Among uncertainties which influence the risk assessment are: a) the likelihood of a spontaneous ignition when the mixture lies in the flammability range of the Shapiro diagram, which affects the hydrogen amounts accumulated before a potential explosion, and b) the possibility of a partial combustion at a high steam rate (30 to 60% vol. of the mixture), initiated by a spark or a hot jet of gas.

The mechanical impact of a hydrogen detonation can be assessed by means of fluid-structure, rapid dynamics interaction codes, such as the CEA PLEXUS code, currently in its final stage of validation. Contrary to the deflagration case, the consequences of a detonation may be strongly dependent on the initiating scenario, mainly the position of the initiator in the containment, due to the multiple reflections of shock waves on the internal structures: this implies the study of a large number of accident scenarios. From a practical standpoint, the potential severity of the effects of a detonation directs the designers and operators towards preventive actions which tend to avoid such events. Currently, the major remaining uncertainty concerns the possibility of establishing a hydrogen detonation regime by flame acceleration mechanisms: such a deflagration-to-detonation transition (DDT) has not yet been properly modelled; available models in France and abroad still lack validation as regards the scaling-up and the extension to complex geometries, so that reliable and non-conservative criteria for DDT cannot be established in terms of hydrogen concentration and geometric boundaries. In short, it is apparent that the key issue for significantly improving our current knowledge on the risk of explosion lies in the flame acceleration and DDT arena. Fully qualified modeling of such phenomena is expected to be a long-term objective, and, for the time being, research efforts could focus on criteria, intended to be valid at the reactor scale, to avoid such a DDT.

As regards the containment behavior, the issue is its leaktightness, both for current parts and singularities, resulting from its thermal-mechanical response to temperature and pressure loads that may arise from the various possible hydrogen combustion regimes. Such behavior is also to be assessed in the case of the impact of missiles resulting from subcompartment or equipment failures.

Concerning the behavior of the instrumentation and systems important for accident management, the objective is to maintain monitoring, and keep control of the containment leaktightness under accident conditions.

From an operational standpoint, the proposals of the plant operators regarding specific hydrogen risk management systems have to be analyzed, in order to assess their capability to properly function under severe accidents conditions. Such systems may comprise on-line devices for the measurement of the hydrogen concentration in the containment, which have been qualified for design-basis accidents only, but need further testing to ensure their reliability under representative severe accident conditions. In addition, the number and the positioning of the hydrogen measurement devices will have to be optimized by the utilities, so as to permit the detection of high local concentrations. Independent technical elements

and methods will have to be acquired or elaborated to allow the IPSN to properly assess all the utility proposals. Other systems proposed may be mitigation systems, such as ignitors or hydrogen recombiners. Such mitigation systems have usually been qualified by the manufacturer for a variety of severe accident conditions; further qualification may nevertheless be required. For example, regarding catalytic recombiners, using tiny porous layers of platinum or palladium on substrates, aging during the normal plant operation is an issue, as well as the early poisoning of the catalyst by airborne chemicals in the containment, including volatile fission products resulting from the severe accident scenario.

1.4 Objective-oriented research prioritization

IPSN priorities are established in line with two orientations: a) risk assessment, and b) the search for and evaluation of operational means to mitigate and manage the hydrogen risk.

Risk assessment provides technical data which allow a more pertinent analysis of: a) the interest of counter-measures in mitigating the hydrogen risk on the various types of operating reactors or designs for the future units, and b) the dimensioning of future reactors.

The above discussion on the major uncertainties affecting the physical phenomena of concern demonstrates that priority should be placed on gaining some additional specific data.

Regarding the hydrogen distribution in the containment, the objective of the research is to obtain experimentally, on a medium scale, the appropriate data for the validation of specific important models for a multi-dimensional code to be developed at the IPSN: such models address steam condensation onto the walls, in the presence of condensable gases, condensation on falling droplets of a water high density spray, and airborne fission product removal by water spraying. The design of the experiments will have to be representative of important reactor and accident features, and extensive and dedicated instrumentation will have to be used, so as to complete existing data from other experiments, as in HDR, at NUPEC or BATTELLE.

As concerns ignition, hot gas jet ignition tests, sponsored by the utility Electricité de France at CNRS-Orléans, as well as USNRC tests at SNL on ignition under operating spray system will enlarge the data base used. Nevertheless, the phenomena of flame acceleration, possibly resulting in a detonation, need to be further investigated experimentally on a scale and in a set of volume arrangements as representative as possible of the reactor case. The various steps of such a study could be the following. First, the investigation of the hydrogen concentration range for which a DDT cannot be reached for hydrogen-steam-air ternary mixtures in a large-scale geometry; second, the development of a code or of a code system, with physically qualified models and adequate numerical methods, able to calculate the sequence of phenomena including the hydrogen distribution in the containment, turbulent combustion, flame acceleration and detonation; third, the identification of existing DDT models, the development of improved ones, when appropriate, and their testing in the above codes; fourth, the specification and the performance of deflagration tests at full scale in a model of reactor subcompartment.

Regarding the assessment of consequences, the effects of hydrogen combustion on fission product behavior in the containment, and on containment integrity should be further examined.

As concerns the search for and the evaluation of operational means to mitigate and manage the hydrogen risk, efforts should be devoted to on-line hydrogen measurement

devices and mitigation systems. For measurement devices, the IPSN objective is to analyze the potential interest of various developing concepts, which can be proposed by the utility for demonstrating its capability to properly monitor the development of the accident conditions in the containment. As regards the mitigation means, IPSN's major concern is to investigate if the hydrogen catalytic recombiners, currently being considered by the utility for a possible equipment of some types of nuclear units, are not vulnerable to poisoning in an airborne chemical environment resulting from a severe accident; when fully qualified, such recombiners could constitute an efficient hydrogen risk mitigation means, theoretically not prone to negative effects when in operation.

2 The IPSN Hydrogen Research Program. National and International Cooperations [1] [2].

Table 1 summarizes the currently envisioned agenda of the IPSN hydrogen research program.

2.1 Hydrogen generation

A key issue, for the realistic evaluation of hydrogen risk and the appropriateness of mitigation systems is to provide a realistic assessment of the hydrogen production and of its production rate. This is to be carried out for various core-melting scenarios terminating in vessel failure. A draft, comprehensive state-of-the-art report [3] has been elaborated by the IPSN, to derive best-estimate values and outline remaining uncertainties and as yet unspecified items to benefit from further research. The major conclusions of the draft report, currently submitted to AVN for a critical review, may be summarized as follows, pending the conclusions of the analysis. The final version is expected to be issued later in English, possibly incorporating revisions and complements.

In stage 1 of core degradation, which is defined as the stage encompassing all phenomena from incipient core heating-up to metallic compound relocation (stage 2), that is, prior to any change in core geometry change, hydrogen generation depends on the oxidation kinetics and the available steam flow-rate. For this stage, code models display a high degree of similarity, and the relevant oxidation laws are in agreement with the results of representative experiments: hydrogen production can be predicted with a fair level of confidence. At the transition between stages 1 and 2, sensitivity studies carried out with the VULCAIN code demonstrate that the thermal-hydraulics of the reactor core and in the primary circuit govern hydrogen generation. A high primary pressure results in limited zircaloy oxidation. If there is some steam condensation in the steam generators, core degradation may be different and hydrogen generation may be higher.

Stage 2 deals with the phenomena involved with metallic compound relocation, prior to ceramic phase relocation. The main uncertainties concern: a) UO₂Zr properties (phase diagram and oxidation kinetics), and b) the relocation of metallic phases.

During the relocation of the ceramic phases (stage 3), further uncertainties appear, in connection with: a) possible channel blockages, b) the radial development of a corium pool, c) the assessment of surface areas prone to be oxidized, in particular for debris beds, d) the oxidation of steel structures, and e) the modelling of fission product release, which affects the energy source. The models used in existing codes appear to be empirical, rather than mechanistic: many inputs have to be chosen by the users. The extrapolation to, full reactor size, of models qualified on the basis of experiments may prove inconvenient due to poor representativity as regards the geometry, and to the procedures used to terminate the

experiments. Attempts to properly recalculate the TMI-2 accident scenario before water injection highlight the difficulties inherent to such predictions.

Stage 4 addresses the last stage of core degradation, from the corium relocation in the lower plenum to vessel melt-through. During this stage, the main uncertainty concerns the amount of materials available for oxidation. As regards zircaloy, only high pressure cases, or rapid kinetics cases after reactor shutdown, may result in extensive non-oxidized masses: hydrogen generation is then limited, first by a low rate of oxidation kinetics, then by steam starvation. Steel may increase the inventory of potentially oxidizable materials if the melting of the core support plate is taken into account. The most important stage as regards hydrogen generation is the corium relocation in the bottom head of the vessel when water is present. The hydrogen generation is limited by: a) the amount of non-oxidized material available, b) the amount of steam which can be produced, and c) the degree of corium fragmentation. When the corium is relocated in the bottom head of the vessel, the amount of steam generated is limited by the quantity of steam which can contribute to the oxidation. In assessments, the duration of stage 4 depends on the model chosen for the vessel failure. As regards stage 4, the amount of hydrogen generated cannot be accurately quantified by codes; only empirical models based on analytical experiments enable some assessments to be made in the framework of sensitivity studies.

Hydrogen generation, in the case of reflooding during stage 1, cannot currently be quantified, either in terms of generation rate or total amount produced. Nevertheless, degraded core experiments such as LOFT or CORA displayed a considerable hydrogen generation. According to the TMI-2 accident analysis, reflooding resulted in the generation of about 160 kg of hydrogen which could not be predicted by the codes used. Reflooding may result in the formation of a debris bed due to the mechanical failure of embrittled fuel rods, depending on the time elapsed since the onset of the accident. The paucity of experimental data does not permit even a qualitative understanding of the phenomena at work in subsequent stages of core degradation.

In short, if the hydrogen production from core heat-up and incipient melting is well understood, later phases need further developments, which are directly linked to core degradation experiments and their modelling (interpretation of PHEBUS FTP0 test with the ICARE code). The core slumping and its interaction with the remaining water in the bottom head of the vessel is also a source of uncertainties, but it is strongly dependent on the earlier phases of core degradation and oxidation. Finally, it is clear that the major problem for some scenarios is the quantification of hydrogen production in case of reflooding. Some experiments (CORA 13, LOFT-FP2) and the TMI2 accident analysis indicate the importance of this phenomenon and the deficiencies of the codes in this field. More experimental work (FZK Quench facility) and theoretical investigations are needed in this field.

2.2 Hydrogen distribution

Studies concerning the distribution and potential stratification of hydrogen are carried out using mainly lumped parameters codes. In the eighties, the IPSN has developed a tool to analyze the behavior of PWR containments under severe accidents conditions: the multiple-compartment JERICHO code of the ESCADRE system. Priority is currently placed on hydrogen distribution assessment in sub-compartmentalized volumes, where higher hydrogen concentration could result in damaging detonative processes.

Moreover, in connection with the layout of mitigation systems, computer simulations of hydrogen distribution in large volumes have to be improved. This is the aim of 3-D codes under development: TONUS at IPSN and ESTETIC at EDF.

The objective of the TONUS code (Figure 1) is to integrate, in a unique and coherent tool, the existing knowledge on the thermal and mechanical behavior of a PWR containment under severe accident conditions. In a first step, modelling will address hydrogen distribution and combustion behavior, whereas the containment dynamic response of the containment to an explosion will be provided by the PLEXUS code.

The diversity of the phenomena to be modelled, the necessity of a strong coupling of the models for these phenomena and the choice of having the code equally suitable for two kinds of users, programming engineers and assessment engineers, have led to the use of the CASTEM 2000 structure as a development basis.

Regarding the containment gas mixture, the method consists in calculating, in the volumes of the multiple compartment geometry, mean values of parameters such as the pressure, the volumic gas mass, various gas concentrations, the temperature, the mass of liquid water and its temperature; flows between compartments are calculated in connecting elements. The system of equations is derived from the balance equations for mass, quantity of movement and energy in the control volumes. In addition, the wall temperatures of concrete and steel surfaces involved in steam condensation are also calculated. As for the modelling of physical phenomena, the models of JERICO will generally be used.

Steam condensation onto the walls in a mixture of steam and non-condensable gases is a phenomenon of prime importance, as, along with the injection parameters, it governs the gas flow patterns in the containment. Three approaches can be used to model steam wall condensation: correlation laws resulting from experiments, e.g. the Tagami-Ushida correlation, which depend on geometry, correlations derived from a mass-heat analogy, such as the Chilton-Colburn correlation, which are independent of the geometry, and the resolution of the coupled heat and mass transfer equations, which implies a detailed meshing of the fluid volume near the wall, essentially inadequate for carrying out safety calculations: the two first correlations will be introduced in TONUS in a first stage.

Regarding the combustion of hydrogen and carbon monoxide in the containment, a model will be used, based on the flame velocity for 0-D approaches. In the meshed volumes, a model of homogeneous, two-phase, compressible fluid will be used; this can be justified by the fact that the mean pressure in the containment is about 5 bar, and, under these conditions, the volumic fraction of liquid water will be small and in the form of small droplets. Non-condensable transport equations will be added to conserve the mass of these species. Turbulence and multi-dimensional combustion modelling will be introduced. Gas stratification, if present, is directly calculated in the multidimensional description of the geometry. Pre-inerting of the containment by a non-condensable gas, such as nitrogen, or attempts of post-inerting by injection of such gases are taken into account as long as initial gas concentrations or the inerting gas injection rate are provided.

A first version of TONUS, for internal use, is expected to be completed by the end of 1996: it will be a multi-compartment, multi-D version, incorporating the detonation model provided by the PLEXUS code and combustion models; a first qualification of convection models should be carried out at the end of 1997, based on available experiments, whereas a full qualification of TONUS, based in particular on the TOSQAN tests, is expected at a later stage.

The validation of such codes needs further experimental results. An analytical experiment COPAIN (CEA/DRN) is designed to determine the heat transfer during steam condensation onto a two-meter high, vertical wall under natural or mixed convection regimes in the presence of non condensable gases. This experiment will be operational in 1996; tests and relevant interpretation should continue through 1999.

The validation of the condensation models onto walls, and also on falling water droplets from an operating spray system, and of distribution models will be performed in a semi-analytical facility of about 8 m³, the TOSQAN facility in IPSN laboratories at Saclay. Such validation needs to be achieved under conditions which are representative of those prevailing in a reactor containment during a severe accident. Measurements of the condensing flow near the walls of the TOSQAN facility should provide data to ascertain the adequacy of the Uchida and Chilton-Colburn correlations to be introduced in the TONUS code.

The design of the TOSQAN facility is based on the following considerations. The height (about 5 m) has been selected to allow for water spraying experiments, complementary of the CARAIDAS tests. In these tests, a fall height of about 3 m appeared necessary to produce a stable flow regime of droplets. Its diameter is limited to about 1.5 m: this results from the laser anemometry technique which will be used for obtaining local values in the gas velocity field, close to the condensation wall. The positioning of the steam and helium injection nozzle can be adjusted between 3 to 5 m under the dome, so as to avoid any interaction between the rising plume and the vessel walls. Two-meter high condensation zones have been situated in the upper part of the vessel, whereas all the walls are temperature regulated. Two types of gas flow patterns will have to be simulated in TOSQAN: turbulent, natural convection, representative of the containment situation, and also a forced convection regime which can be encountered in sub-compartments: these regimes can be obtained by regulating the gas flow velocity at the injection nozzle. Currently, the TOSQAN project is in its specification stage as regards its dimensioning and instrumentation: the containment aspects, of the severe accident scenarios to be simulated on French PWRs are being examined; investigations about the feasibility of various methods for measuring the fields of concentrations near the walls are on-going. According to current estimates, the first tests in the TOSQAN facility are scheduled for 1998.

2.3 Hydrogen combustion

High temperature flammability studies have been carried out by EDF in collaboration with the CNRS at Orléans: various air-H₂-steam mixtures in the 20°-130°C temperature range, were ignited by a hot (> 1500°C) gas jet produced by a shock tube. The results indicate that the flammability limits of such ternary mixtures are broadened as the jet temperature increases.

Priority is now placed on flame acceleration mechanisms and on the deflagration-to-etonation transition (DDT), which could possibly occur in the containment sub-compartments. Complex scaling effects are expected to preclude the implementation of a full modelling of transition phenomena in the coming years; accordingly, as a first step, research efforts will be focused on criteria for preventing any DDT.

A joint research program, based on relatively large-scale (500 m³) DDT experiments including partial steam pressure is currently being performed in the RUT facility in Russia [4]. The main objectives of this program, supported jointly by the IPSN, FZK, and the Kurchatov Institute, are:

- to determine the transition limits for various homogeneous, air-hydrogen-steam mixtures;
- to verify the adequacy of a DDT criterion, deriving from the analysis of previous experiments; such criterion is $L = 7 \cdot \lambda$, where L is a dimension, representative of the size of the test facility, and λ is the size of the detonation cell of the mixture.
- to study turbulent flame behavior and the resulting loads for this type of mixture

Six tests with ternary mixtures have been already performed with various $H_2/(H_2+air)$ ratios, encompassing the expected « critical » value for a DDT, for given H_2O concentrations, resulting - or not - in a DDT. A comprehensive report is to appear by the end of June 1996, and the physical interpretation will continue in 1997. The program of the next test series is currently being finalized by the partners, joined by USNRC. One possible direction for research is to extend investigations on the DDT limit to higher hydrogen and steam concentrations, so as to obtain a full picture of the transition limit in the realm of interest.

2.4 Structure response

Concerning detonation, the PLEXUS code calculates the propagation of a shock wave in the containment and its interaction with structures. The PLEXUS code will be further validated with FZK shock tube experiments. The new TONUS code will incorporate some PLEXUS models calculating the thermal and mechanical loadings, but the structural response should continue to be calculated by the PLEXUS code, or CASTEM for quasi-static loadings, for the time being.

2.5 Mitigation. Hydrogen catalytic recombiners.

As previously mentioned, the TONUS code should provide guidance, in the late nineties, for an optimized positioning of hydrogen measuring devices and/or hydrogen depleting devices like recombiners, should their potential for significantly improving the level of safety be established on technical grounds for specific reactor designs. Pending the full qualification of TONUS, if an early decision were to be made to install catalytic hydrogen recombiners on some types of French PWR units, theoretical investigations using less accurate but available codes like JERICO or RALOC, along with well-founded engineering judgment, would be necessary to determine the main flow paths of the gases in the containment relevant to the optimum position of the catalytic recombiners. In addition, a full demonstration of how effectively such systems work during accident scenarios should be carried out. Some catalytic recombiner testing under severe accident conditions (including spray actuation) is performed in the KALI facility (CEA/CADARACHE).

The potential poisoning mechanisms of catalytic recombiners in adverse environments representative of severe accidents conditions will be studied in the H2PAR facility at CEA/CADARACHE (IPSN). These tests, which use a representative choice of fission product simulants, should begin in 1996 and be fully completed in the year 2000. The twenty-four fission product or core material simulants of interest, in the form of aerosols and vapors, will be generated by the high-temperature (up to 2900 °C) POLYR induction furnace. The test matrix, as currently envisioned, comprises preliminary tests for aerosol characterization, to be compared with HEVA-VERCORS and PHEBUS-FP results, and full-scale experiments, first, without aerosols, to qualify the H2PAR facility and establish the « reference » catalytic efficiency regarding hydrogen removal, and second, tests with aerosols and vapors, to assess the sensitivity to poisoning of the catalyst, whether cold or hot. The results of those experiments (KALI, H2PAR) will be integrated in improved catalytic recombiner models used in thermal-hydraulic containment codes.

3 Conclusion

The IPSN has launched a comprehensive research and assessment program on key hydrogen behavior uncertainties that may strongly influence the decision, whether or not to implement counter-measures to deal with the issue of hydrogen risk in the containment.

As regards the realistic assessment of hydrogen production kinetics and cumulative amount, together with its release conditions into the containment, a significant analysis effort has been developed at IPSN, taking into account the latest data available and the relevant reactor specificities. Large variations appear to exist, depending on the accident scenario; uncertainties due to oxidation models or 3-D effects in the core are significant. The uncertainties on hydrogen generation, after corium relocation in the bottom head of the vessel also depend on the amount of structural materials, such as the steel of the support plate, that are mixed with the core materials. In addition, the impact of core reflooding on hydrogen generation in the early stages of degradation can only be derived qualitatively from specific experiments. In conclusion regarding hydrogen generation, relatively rapid kinetics may be expected in the early stages of the accident scenario, governed by the steam flow rate available for leaching exposed high temperature zircaloy surfaces, followed by a slower oxidation of the corium, in relation to oxidizable material availability (including structural materials), exposed surfaces (relatively low surface/volume ratios for the corium), and steam availability. The physical assessment of the hydrogen generation rate can be expected to be out of reach for a long time in reactor calculations. Only rough cumulative amounts can be estimated for the various stages of severe arrested in-vessel accident scenarios, with the greatest uncertainties appearing as soon as the corium relocates to the bottom head of the vessel, until it is ultimately cooled down. Efforts are still to be made on production for the late core degradation phase and core reflooding. Further code developments in relation with core degradation experiments are necessary, and validation of in-vessel flooding models in degraded geometries has to focus on hydrogen production phenomena.

As regards hydrogen distribution, and a possible stratification risk, the IPSN TONUS, physical code, currently under development, should provide guidance, in the late nineties, for an optimized positioning of hydrogen measuring devices and/or hydrogen depleting devices like recombiners, should the decision of installing them be taken for specific reactor designs. Pending TONUS development and qualification, were an early decision to be made to install catalytic hydrogen recombiners on some types of French PWR units, theoretical investigations using available codes like JERICHO or RALOC, along with a well-founded engineering judgement, would be necessary to assess the main flow paths of the gases in the containment, relevant to the optimum location of the catalytic recombiners.

Nevertheless, regarding hydrogen evolution and distribution in the containment, priority is placed on sub-compartments where high hydrogen concentrations could result in a detonation, either directly, or by deflagration-to-detonation transition.

Concerning DDT, it is apparent that a full comprehension of the transition phenomena, supported by validated flame acceleration mechanisms, would entail years of sustained, dedicated research; therefore, priority has been placed on setting criteria which delimit the non-transition realm for representative air-steam-hydrogen mixtures at high temperature; IPSN participation in the RUT experiments, in collaboration with FZK, USNRC and the Kurchatov Institute, is expected to provide shortly comprehensive data for establishing a criterion excluding any DDT, for realistic severe accident conditions.

As to a better assessment of the hydrogen distribution between sub-compartments, as well as to the pressure-time history onto the walls in the case of either a deflagration or a detonation, the new 3-D, meshed TONUS code is currently under development at CEA for IPSN, based partly on models already existing in the TRIO-EF/VF, CASTEM 2000 and PLEXUS codes.

Some highly important thermal-hydraulic models, like those for wall condensation and aspersion, will be validated by various analytical experiments and by specific tests under representative severe accident conditions in the projected IPSN TOSQAN facility at Saclay. TONUS is expected to be operational and appropriately qualified by the end of the nineties. IPSN has simultaneously launched an experimental program to ascertain whether some existing measuring devices and/or recombining systems can remain efficient under severe accident conditions, and do not cause adverse effects: the H2PAR facility at Cadarache should provide such representative environments, with fission product simulants, to complete their qualification by the end of the nineties. This two-level strategy will make it possible to be prepared: a) to instrument some types of existing French PWRs, starting in 1998, should the utility proposal, still at the level of a principle for P4 and N4 type units, be approved by the safety authority, and b) to provide enough qualified tools to contribute to optimize the designs of future projects to be built beyond the year 2000, for which severe accident prevention and mitigation will be an integral part of the design.

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H₂ PROGRAM TIME SCHEDULE

	1995	1996	1997	1998
GPR Meetings on Severe Accidents		May meeting		
Level-2 PSA				
H2 Generation	Synthesis Report			
H2 Distribution	Expertise	PWR Geometries Existing means (RALOC)	TONUS 1.0 Applications	TONUS 2.0 Applications
TONUS Code	Specification	TONUS 1.0	TONUS 2.0	TONUS 2.0
COPAIN TOSQAN				First experiment
RUT	Tests with steam (2nd test series)	Third test series Reporting and criteria to prevent DDT	Reporting and criteria to prevent DDT	
Combustion Models	Literature Survey (models and criteria)	RUT interpretation Choice for TONUS	Validation (RUT, 4th PCRD : TUM)	
Recombiners	KALI	First tests with F.P. simulants in H2PAR Calc. with existing means (RALOC, JERICHO)	H2PAR 2nd test series	H2PAR 2nd test series
H2 Measurements	Prototype Apparatus for TOSQAN (conc. field measurements)	Prototype Apparatus for TOSQAN (conc. field measurements)	Prototype Apparatus for TOSQAN (conc. field measurements)	



Hydrogen Distribution

TONUS Code

→
Main Mass and/or
Energy Transfers

[Chilton-Colburn(δ)
COPAIN Model
Stable Film Model

Liquid
Transfers
Immediate
(no water running)

Aerosols
Monodisperse
Power sources

Steam
Condensation

Aerosols

γ

$\beta\gamma$

GAS (H₂O+non-condens)

Removal

Air/Water

Sources
Boundary
Conditions

Water Spraying

Two-fluid Model

Droplets

Compressible
Two-phase
Homogeneous

Turbulence

- Constant viscosity NS
- (k,E), mixing length

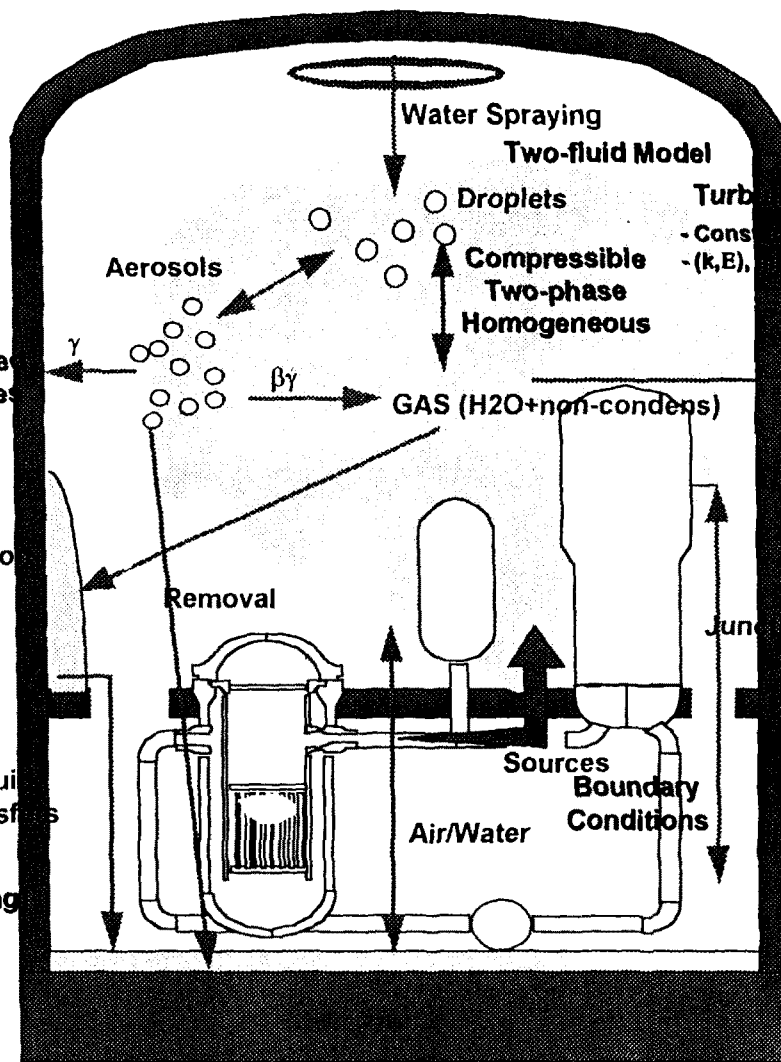
Leaks

Meshing

1-D Conduction

Junctions

Meshing



1.3 RECENT WORK IN SWEDEN CONCERNING THE HYDROGEN ISSUE IN RINGHALS PWRs

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This report gives an overview of recent Swedish activities concerning the hydrogen issue. Our plans for the near future are also shortly described.

Last year a study was performed in order to estimate the probability of containment failure due to hydrogen deflagration after a severe accident in Ringhals 3, a three loop Westinghouse PWR. This investigation was based on a PSA level 1 study providing the core damage frequency in different scenarios.

Given a core melt has occurred the situation was analysed using event trees in the two intervals before resp. after reactor vessel failure. The probability of failure of the containment is extremely low (depending on too low hydrogen concentration) before reactor vessel meltthrough. However, if the hydrogen produced before vessel failure is accumulated the total hydrogen concentration after vessel failure will be higher and if a deflagration occurs the failure pressure of the containment might be exceeded. Hence the probability of containment failure due to hydrogen deflagration depends strongly whether a deflagration occurs before vessel failure or not. The way of using the containment spray has also a strong influence on the results. If the spray is started steam will condense and if an ignition source is present and the concentration of hydrogen is sufficiently high it will burn. The result of the study was an estimated value of the probability of containment failure of less than $1.E-7$ /year using conservative assumptions.

The probability of containment failure due to hydrogen deflagration could be strongly reduced by introduction of catalytic recombiners. However, in our plants it is possible to release hydrogen to the atmosphere via the filtered venting system. This method does not require installation of any extra equipment in the containment and will be investigated in a separate project during 1996.

1. INTRODUCTION

This report gives an overview of work in Sweden in the last three years about the hydrogen issue. The main activity was a study in order to estimate the probability of containment failure caused by hydrogen deflagration after a severe accident in Ringhals 3, one of our PWR:s (three-loop, Westinghouse design) at the Ringhals site on the Swedish west coast.

In our plants it is possible to use the filtered venting system to release hydrogen in the containment to the atmosphere. A study of this possibility as a mitigating measure will be performed during this year.

2. OBJECTIVE

The objective of the study presented in this paper was to estimate the probability of containment failure in Ringhals 3 due to hydrogen deflagration after a severe accident. The calculations were carried out for two cases with respect to input data: best-estimate resp. conservative data.

3. METHOD

3.1 General

The probability of containment failure due to hydrogen deflagration can be split up in the following two factors:

- The probability of hydrogen deflagration
- The probability of containment failure given that a hydrogen deflagration occurs

To analyse the problem we have used an event-tree technique. One event-tree is used before reactor vessel failure and another event-tree is used after vessel failure. These are shown in figure 1 and 2.

3.2 Probability of Hydrogen Deflagration

A hydrogen deflagration occurs if the following two conditions are fulfilled: the atmosphere in the containment is burnable and an ignition source exists. To satisfy the first condition a large amount of hydrogen must be produced during the accident. Furthermore, if the steam concentration in the containment is low enough (below 55% by volume) the gas mixture in the containment will be burnable.

To start a hydrogen deflagration an ignition source must exist. However only a very small spark is needed. Therefore the probability of hydrogen deflagration is high if the atmosphere in the containment is burnable.

3.3 Probability of Containment Failure Given that Hydrogen Deflagration Occurs

The probability of containment failure at hydrogen deflagration is determined by the overlap between the distribution curve for containment pressure at deflagration and the failure pressure curve of the containment.

The pressure in the containment at a deflagration depends on the pressure before deflagration, available amount of hydrogen in the containment and the completeness of the burn. In this study we assume complete burning of available hydrogen. It is also assumed that no hydrogen is lost from the containment via the filtered venting system or leakage.

The amount of hydrogen generated in the accident is mainly dependent on the fraction of oxidized zirconium. A small contribution from steel is also included. We make a difference between hydrogen produced before resp. after reactor vessel failure.

3.4 Calculation of Probability of Containment Failure

We introduce the following notations:

P1= The probability of containment failure before reactor vessel failure

P2= The probability of containment failure after vessel failure

P1b= The probability of deflagration before vessel failure

P2b= The probability of deflagration after vessel failure

F1= The overlap between the distribution curve for containment pressure at a deflagration and the failure pressure curve of the containment.

F2= The corresponding overlap after vessel failure

The probabilities P1 and P2 are calculated as follows:

$$P1 = P1b * F1 \text{ and } P2 = (1 - P1b) * P2b * F2$$

Calculations are performed for the six scenarios in section 4. The total probability of containment failure is the sum of the contributions from the six cases. The calculations were carried out by use of best-estimate resp. conservative data.

4. ACCIDENT SEQUENCES

Given an accident sequence the probability of containment failure due to hydrogen deflagration is the product of the following two probabilities:

- The probability of the sequence
- The probability of containment failure in this sequence

In the PSA level 1 study for Ringhals 3, [1], the accident sequences with core damage are given. A large fraction of these include core melt-down and reactor vessel failure. These cases have been condensed to six typical sequences by using the following criteria:

- Similar initiating event
- Comparable time scale between initiating event and vessel failure
- Quantity of hydrogen produced before vessel failure
- Amount of water in the cavity below the reactor at vessel failure

The probability of vessel failure for each sequence is given in [1] and shown in table 5. The time for vessel failure (from initiating event) and the amount of water in the cavity have been calculated by MAAP, [2]. The cavity contains water at vessel failure except in the sequences T3LX4 and T5D1X1.

In the following a very brief description of the sequences is given.

AX6- the initiating event is a large LOCA. The coolant inventory is lost very quickly. Cooling of the core by recirculating of sump water fails because of human error.

S1H1- the initiating event is an average size LOCA. Reduction of the primary pressure and high pressure safety injection are assumed to fail.

S2D1D5- the initiating event is a small LOCA. Neither high pressure nor low pressure injection works. When the accumulators are empty the pressure starts to increase and the core uncovers.

S2H2- the initiating event is a small LOCA. The pressure in the primary system is reduced but the low pressure recirculation does not work. The core is cooled by water from the RWST but when this tank is empty the core uncover starts.

T3LX4- the initiating event is loss of feedwater. It is also assumed that auxiliary feed water is unavailable. Feed and bleed (by safety injection and relief of steam via the pressurizer) is not started in time to avoid core melt.

T5D1X1- the initiating event is a steam generator tube rupture. The pressure in the primary system is not reduced (human error) and safety injection does not work. In this case at least 50% of the hydrogen is released via the damaged steam generator tubes.

5. DATA

5.1 Hydrogen production

The hydrogen quantities used in this paper are estimated values based on best knowledge. To obtain hydrogen distributions we need 48 values of amounts of hydrogen according to the following:

- In-vessel resp. ex-vessel
- Best-estimate resp. conservative
- Maximum resp. average
- Six sequences

The maximum and average values are needed to obtain probability distributions for the quantity of hydrogen in the containment.

In-vessel hydrogen production

During meltdown of the fuel large amounts of hydrogen are produced when zirconium reacts with steam. However based on [3] the oxidation cannot be complete for the following reasons:

- In the assemblies in the outer part of the core the fraction of cladding oxidation is low because of low temperature.
- Especially at the bottom of the core the availability of steam is limiting for oxidation of cladding.

The hydrogen production is also dependent on accident sequence. Reduction of the pressure in the primary system increases the steam production and also the hydrogen production. Reflooding of the core increases the hydrogen production.

The values used for in-vessel hydrogen production are summarized in the following table.

Table 1

Sequence	Best estimate values Hydrogen, kg		Conservative values Hydrogen, kg	
	Average	Max.	Average	Max.
AX6	250	400	350	500
S1H1	350	500	450	600
S2D1D5	350	500	450	600
S2H2	350	500	450	600
T3LX4	300	450	400	550
T5D1X1	300	450	400	550

Complete oxidation of all zirconium in the core corresponds to a hydrogen production of 760 kg.

Ex-vessel hydrogen production

We take the following contribution to hydrogen production after reactor vessel failure into consideration:

- Oxidation of residual zirconium and steel in the melt. In case of water in the cavity there is a direct interaction with water and if the cavity is dry hydrogen is produced by core-concrete interaction.
- Oxidation of aluminium and zinc in the containment, which is a very slow reaction.

Hydrogen generation during fuel/coolant interaction has been investigated experimentally, [4]. Results from core-concrete interactions are presented in [5]. The amount of steel in the melt is about 4 tonnes, [6].

In our case the following fractions are used for oxidation of remaining zirconium and steel in the melt: best-estimate average=20%, best-estimate maximum=40%, conservative average=40%, conservative maximum=60%

The following hydrogen quantities from oxidation of aluminium and zinc are also added: best-estimate average 20 kg, best-estimate maximum 40 kg, conservative average 40 kg, conservative maximum 80 kg

The hydrogen quantities after vessel failure are obtained by summing the values in table 1 and the ex-vessel contributions given above. The hydrogen quantities after vessel failure are summarized in table 2.

Table 2

Sequence	Best estimate values Hydrogen, kg		Conservative values Hydrogen, kg	
	Average	Max.	Average	Max.
AX6	410	660	630	860
S1H1	490	720	690	900
S2D1D5	490	720	690	900
S2H2	490	720	690	900
T3LX4	450	690	660	880
T5D1X1	450	690	660	880

5.2 Pressure in the Containment at Hydrogen Deflagration

The pressure at hydrogen deflagration is the sum of the pressure before deflagration and the pressure increase due to the deflagration. The pressure in the containment at hydrogen deflagration has been calculated by use of the CONTAIN code. These calculation have been reported in [7]. By use of these results the containment pressures corresponding to the hydrogen quantities given in table 2 have been determined. The values are summarized in table 3.

Table 3

Sequence	Best estimate values Pressure, bars		Conservative values Pressure, bars	
	Average	Max.	Average	Max.
AX6	5.0	6.2	6.4	7.6
S1H1	5.4	6.5	6.7	7.8
S2D1D5	5.4	6.5	6.7	7.8
S2H2	5.4	6.5	6.7	7.8
T3LX4	5.2	6.3	6.6	7.7
T5D1X1	5.2	6.3	6.6	7.7

In the sequence T5D1X1 at least 50% of hydrogen generated before vessel failure will be released via the damaged steam generator. This is not corrected for in table 3.

5.3 Probability of Hydrogen Deflagration before Reactor Vessel Failure

Two conditions must be fulfilled to initiate a hydrogen deflagration:

- The gas mixture is burnable.
- An ignition source exists.

The gas mixture is burnable if the hydrogen concentration is above 4%, the oxygen concentration above 5% and the steam concentration below 55%.

If the hydrogen concentration is below 10% measured in dry air in the containment the containment spray will be used to reduce the steam concentration and (if ignition occurs) start a deflagration. This is part of the accident management strategy to eliminate hydrogen from the containment. However it is limited to cases with a hydrogen concentration less than 10% otherwise the filtered venting system will be activated or (in case of very high hydrogen concentrations) the failure pressure of the containment can be exceeded.

If the hydrogen concentration is in the range 4-10% the probability is high that a deflagration starts after reduction of the steam content by use of the containment spray. This probability is estimated to be in the range 0.8-0.95. It increases with the time elapsed from the initiating event to reactor vessel failure. The values used in the calculation of the probability of containment failure are included in table 4, section 6.1.

5.4 Probability of Hydrogen Deflagration after Reactor Vessel Failure

The probability of hydrogen deflagration after reactor vessel failure depends on the following:

- If a hydrogen deflagration has occurred before vessel failure.
- If the atmosphere in the containment is burnable.
- If an ignition source exists.

If a hydrogen deflagration occurs before vessel failure the amount of hydrogen available for burning after vessel failure is reduced. If at least 200 kg is burned in the first deflagration the pressure peak in the second deflagration will not reach the failure pressure of the containment.

The atmosphere in the containment will be burnable if the steam concentration in the containment is below 55% by use of the spray system. If this is the case with a hydrogen concentration above 10% there is a certain probability of containment failure. The probability of using the spray under these circumstances has been estimated to 0.1, the same value in all of the six sequences.

The probability that an ignition source exists is high and the value 1.0 has been assumed in the calculations.

5.5 Containment Failure Pressure

The following values for the failure pressure of the containment are given in [8]:

- At pressures below 6.7 bars (abs) the probability of leakage is very small.
- At a pressure of 6.9 bars the probability for containment failure is high.

It is assumed that the probability of containment failure increases (linearly) from zero at 6.7 bars to 1.0 at 6.9 bars.

6. CALCULATIONS

The data given in section 5 are used for calculation of the probability of containment failure due to hydrogen deflagration. The method is described in section 3 above. The maximum amount of hydrogen before vessel failure is 600 kg. If this quantity is completely burned the pressure peak will be about 6 bars, which is below the failure pressure of the containment.

In table 3 best-estimate data give pressures below the failure pressure. Therefore the following calculations are performed with conservative data using hydrogen quantities after reactor vessel failure.

6.1 Conditional Probabilities of Containment Failure

As described in section 3 the probability of containment failure after vessel failure is given by the following: $P2 = (1 - P1b) * P2b * F2$. The overlap, F2, has been calculated for the different sequences by using the values in table 3 and the failure pressure curve. Values of P1b, P2b and F2 are given in table 4.

Table 4

Sequence	P1b	P2b	F2	P2
AX6	0.80	0.1	0.22	0.0044
S1H1	0.80	0.1	0.42	0.0084
S2D1D5	0.90	0.1	0.42	0.0042
S2H2	0.95	0.1	0.42	0.0021
T3LX4	0.80	0.1	0.34	0.0068
T5D1X1	0.80	0.1	0.34	0.0000

6.2 Probability of Containment Failure

In each of the six sequences the probability of containment failure is the product of the following two probabilities:

- The probability of reactor vessel failure in the sequence, see table 5.
- The probability of containment failure given that vessel failure has occurred, see table 4.

The results are summarized in table 5.

Table 5

Sequence	Probability of vessel failure	Conditional probability of cont. failure	Probability of cont. failure
AX6	1.0E-6	4.4E-3	0.44E-8
S1H1	2.0E-6	8.4E-3	1.68E-8
S2D1D5	0.4E-6	4.2E-3	0.17E-8
S2H2	4.7E-6	2.1E-3	0.99E-8
T3LX4	3.5E-6	6.8E-3	2.38E-8
T5D1X1	2.6E-6	0.0	0.00E-8
Sum:	1.4E-5		5.7E-8

The conclusion is that the probability of containment failure due to hydrogen deflagration is 0.6E-7/year based on conservative data.

7. DISCUSSIONS AND CONCLUSIONS

The probability of containment failure depends mainly on the following quantities:

- The amount of hydrogen produced during the accident
- The probability of hydrogen deflagration before reactor vessel failure
- The probability that containment spray is used in spite of high hydrogen concentrations in the containment

The probability of containment failure due to hydrogen deflagration has been conservatively estimated to $0.6E-7$ /year for Ringhals 3.

8. CONTINUED WORK

All Swedish plants are equipped with containment filtered venting systems. In case of a severe accident the venting system can be used to prevent slow overpressurization of the containment.

However, it is also possible to partially release hydrogen in the containment to the atmosphere by manually activating the venting system. This method to resolve the hydrogen issue will be investigated in a separate study during this year. An important aspect here is the influence of hydrogen stratification on the release of hydrogen via the venting system.

In this study the MAAP4 code will be used. Different severe accident sequences will be analyzed with respect to initiating event and amount of hydrogen produced in the containment. In each sequence the filtered venting system is activated and the composition of the atmosphere after venting is calculated.

Up to now MAAP-calculations for a total black out sequence have been performed. Results from this case indicates that it is possible to reduce the hydrogen content in the containment sufficiently to avoid overpressurization caused by deflagration.

9. REFERENCES

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Figure 1

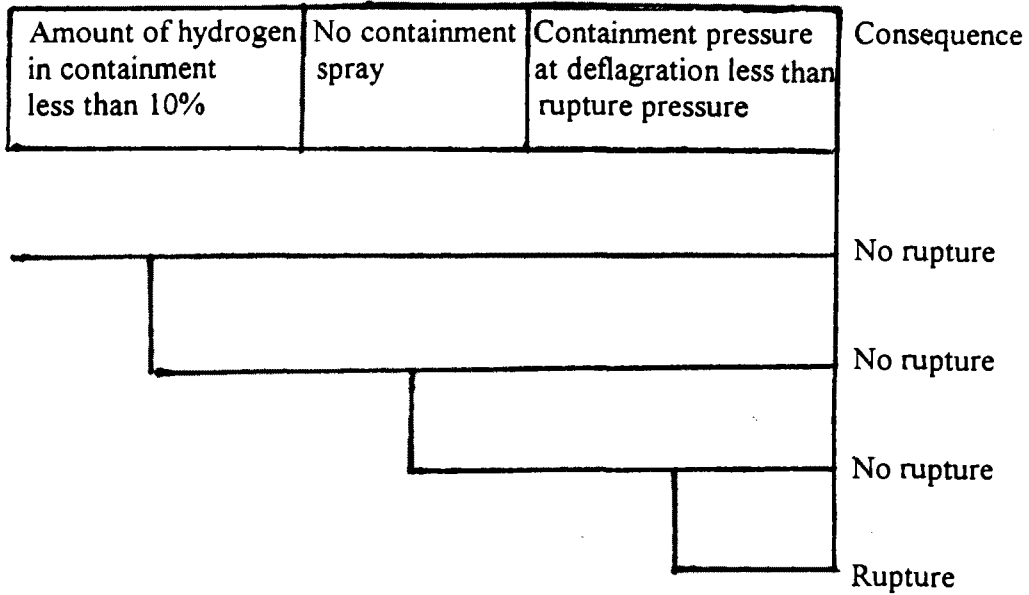
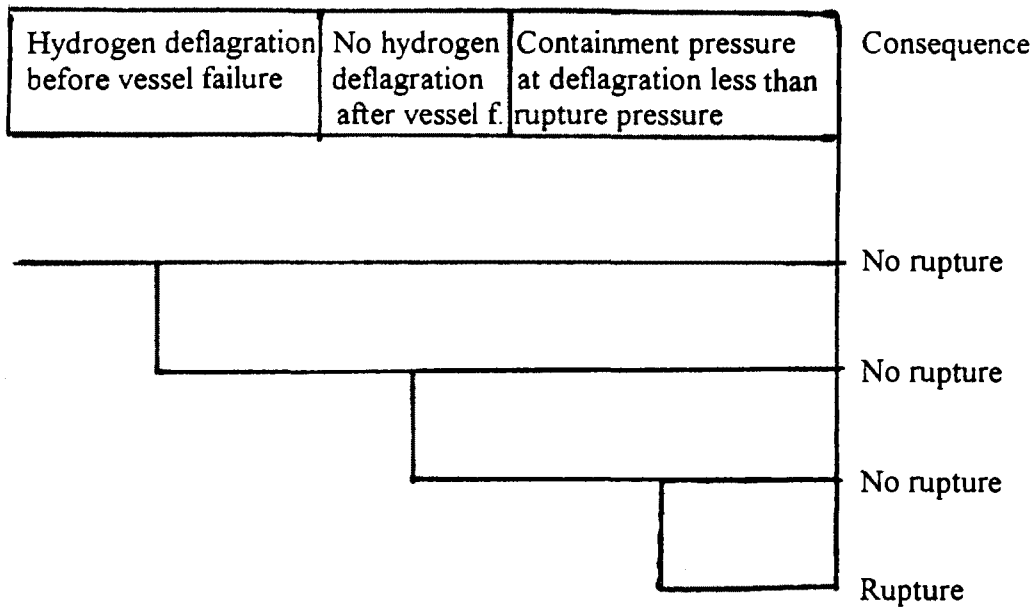


Figure 2



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1.4 EDF ANALYSIS OF HYDROGEN PROBLEM ON PRESENT NPPs

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ABSTRACT

By letter dated March 1995, Electricité de France (EDF) has sent to the French Safety Authority (called DSIN, depending jointly from Industry and Environment Ministries) its position, for Present NPPs, regarding three main topics concerning severe accidents pointed by DSIN :

- hydrogen problem,
- high pressure core melt risk,
- instrumentation.

Concerning "hydrogen problem", EDF answer is not only a state-of-the art analysis but intend to answer the question of the need (or not) for implementation of countermeasures in Present French NPPs.

This position is presently under review by DSIN and its Technical Supports.

1 Introduction : frame of EDF analysis

This paper is a synthesis of EDF analysis presented to DSIN in March 1995 in order to answer the two following questions concerning risk connected to hydrogen generation in French Present NPPs containment building during hypothetical severe accident sequences :

- is there a need for countermeasures implementation ?
- what is (are), in case of necessity, the most pertinent countermeasure(s) ?

First, it is important to note that present state of fundamental knowledge concerning possible hydrogen combustion modes does not allow to perform a technical analysis leading to a fully justified and quantified practical solution.

This is particularly true for flame acceleration and deflagration-to-detonation transition (DDT) phenomenon for which no modelization, and consequently no demonstrated criterion, is presently available.

In addition to this incomplete knowledge for basic physical phenomena, uncertainties concerning in-vessel core degradation and hydrogen production (particularly in case of core recovery) have to be taken into account.

From this, present state of international thought, including effective operational decisions, is important part of the analysis.

As a general principle for severe accidents, EDF does not intend to take into account, in operational term, mitigation techniques not to be subject to large international technical consensus.

This EDF position is presently under review par DSIN and its Technical Supports.

2 Need for countermeasures in Present French NPPs

To answer this question needs to assess the following technical items :

- relevant hydrogen amount,
- relevant hydrogen combustion modes,
- effect of combination of the two above items in term of containment resistance.

As a general principle for severe accidents analysis, answers to these questions have to be determined on a realistic ("best estimated") basis. This is a difference with design basis accidents (DBA) analysis which are performed using conservative assumptions.

2.1 Hydrogen amount

Potential hydrogen generation sources in severe accidents are :

- in-vessel : mainly oxidation of fuel cladding zirconium by steam,
- ex-vessel : oxidation of remaining zirconium and core concrete interaction,
- long time : water radiolysis.

These two typical hydrogen generation phases, which are time successive, have not the same level of probability occurrence, but accumulation of hydrogen coming from these different sources is a potential threat for containment integrity.

Present state of finalization of EDF PRA level 2 has not allowed its use for determining the realistic hydrogen amount.

Consequently, input data used by EDF for assess the cumulative hydrogen amount had been :

- proper typical accidental sequences calculations using MAAP 4 (reference /1/),
- inventory of values internationally proposed for present or future NPPs (for example : USA from EPRI and US/NRC, Germany from RSK and SIEMENS, Europe : from EUR requirements for LWR NPPs ...).

Considering French Present Plants operation general organisation (including training on simulators), conditional probability of in-vessel core recovery in case of severe accident is high.

Consequently, EDF take, as a reference value, the realistic cumulative amount of hydrogen for in-vessel core recovery sequences.

According to the input data mentioned above, this cumulative amount corresponds to oxidation of the equivalent of 75% of the part of the zirconium fuel cladding elements surrounding the active part of the core (75% PAG). It has to be pointed that this notion of "PAG" oxidation has not to be understood in a technical way but only as a reference (physically, others areas of the total zirconium core inventory can be oxidised by steam).

Nevertheless, for "defence in depth" purpose and by reference to proposed values for Future Plants the analysis take into account, as a maximal realistic value, hydrogen cumulative amount corresponding to 100% PAG.

This last value being used, associated with reference accident sequences, for design of hydrogen mitigation systems for US ALWRs and common German/French EPR.

2.2 Hydrogen combustion modes

The most likely hydrogen combustion mode in a PWR containment building is deflagration.

There is an international consensus to assess that it is highly unlikely that a source of sufficient energy to cause a direct detonation would be available in the containment building of a PWR Dry Containment type (all French PWRs are of this type).

Consequently the only realistic potential detonation mode in French PWRs appears to be DDT.

According to the lack in basic knowledge concerning this phenomena mentioned above, it is presently not possible to have a peremptory judgement concerning the occurrence of this phenomenon and EDF analysis is the following :

- it is not possible, by principle, to exclude the possibility of a DDT. But, if we look at the general geometric configuration of French PWR containments, it appears that DDT should be highly unlikely in the "upper part" which is quite obstacles free ("dome area") and could only not to be excluded in the "lower part" (area of "reactor coolant rooms") which is also the area of hydrogen emission,
- French PWR containments are relatively "open geometry" which intend to improve hydrogen concentration homogenisation and reduce the probability of DDT,
- in case of postulated DDT initiated in a "reactor coolant room" (as explained above) :
 - * transmission, as a detonation, to the "dome area" appears - according to surface area of connecting parts - to be unlikely for the maximal considered hydrogen amount (corresponding to 100% PAG),
 - * first evaluations intend to show that a postulated missile effect resulting from this local DDT should not be an evident threat for inner containment (presstressed) wall integrity.

2.3 Hydrogen risk EDF analysis

According to previous hydrogen combustion mode analysis, EDF considers only deflagration effect for risk analysis.

Table 1 gives realistic (including heat absorbed by internal containment structures) containment peak pressure in case of global deflagration of hydrogen amount corresponding to 75% PAG and 100% PAG.

The calculation gives the maximal value (according to Shapiro diagram limits) in case of initial steam saturated containment atmosphere.

These peak pressure values have to be compared with the corresponding estimated containment "ultimate" resistance.

Presently, the estimated "ultimate" containment resistance values are :

- CP1/2 (900MWe, 3 loops, simple wall with metallic liner) : 9,0 bar (absolute) for a design pressure of 5,0 bar (absolute),
- P4/P'4 (1300MWe, 4 loops, double wall containment without liner) : 7,5 bar (absolute) for a design pressure of 4,8 bar (P4) or 5,2 bar (P'4),
- N4 (1400MWe, 4 loops, double wall containment without liner) : 7,5 bar (absolute) for a design pressure of 5,3 bar.

For all French NPPs, inner containment wall is made with prestressed concrete.

Studies or tests, concerning mainly containment devices such as hatch or isolation valves, are still in progress to reinforce these values.

The comparison between deflagration peak pressure and "ultimate" containment resistance shows that all French NPPs appear to be able to withstand, with important margins, realistic hydrogen deflagration corresponding to the reference quantity of 75% PAG.

These margins are greater than necessary to comply with hydrogen deflagration of 100% PAG for CP1/2 and P4 Plants. In the case of P'4 and N4, no additional margins are available for this case.

Consequently EDF analysis conclusion is the following :

- implementation of additional countermeasures to cope with hydrogen risk is not required,
- nevertheless, for "defence in depth" purpose, installation of catalytic recombiners (see § 3) is studied for P'4 and N4 Plants. These recombiners will be effectively fitted if studies performed confirm their efficiency in representative conditions and do not led to drawback consequences.

In addition, to improve accident management, EDF intend to install, in case of positive result of equipment test programme presently performed, internal reactor building hydrogen concentration measurement using 4 localisation.

3 Choice of potential hydrogen countermeasure(s)

Three techniques had been evaluated :

- use of igniters,
- containment atmosphere inerting,
- use of catalytic recombiners.

3.1 Use of igniters

This solution is presently excluded according to the lack of knowledge concerning DDT phenomenon. It appears not possible to determine real consequences of igniters installation in reactor building complex geometry.

Consequently, for mean time operational decision, this technique could not be selected.

3.2 Containment atmosphere inerting

Two techniques are proposed :

- pre-inerting,
- post-inerting.

Pre-inerting consists in replacing containment air content by an inert gas during plant normal operation to avoid any hydrogen combustion risk. This mitigation technique is used on BWR Mark I and II.

For the induce risk to workers during plant normal operation (anoxia) EDF does not consider this mitigation technique (according to anoxia risk, EDF has strongly reduced, on the past, the use of inert gases on NPPs).

In addition, it has to be noted that this mitigation technique is not a complete solution because oxygen and hydrogen long term production by water radiolysis needs a complementary countermeasure.

Post-inerting consists in sufficient containment inert gas injection at the onset of the accident to avoid - or limit - hydrogen combustion risk.

EDF analysis is that this technique, which can appear as a good solution in the principle, is a difficult and strong technique in practical (including operational) term.

For example, the main problems are not so evident to solve :

- choice of the signal, initiating the inerting sequence which has to be early and fast,
- hard detailed design of the solution (in particular, a lot of hydrogen repartition calculations are needed) which, in practical, needs a pipes network in the containment building to take into account various potential hydrogen sources localisation,
- risk induces by opening and closing of a containment penetration during the accident. This problem being able to be more stringent in case of use of cryogenic inert gas. In this case, potential effect of cold liquid gas spray on containment building equipment has to be studied or prevented,

- internal containment pressure will be increased that could lead to earlier opening of containment venting system ¹ and, consequently, increase of radioactive external releases.

In addition, the use of this technique appears at the opposite of EDF practice to strongly limit the use of inert gases on NPPs.

3.3 Use of catalytic recombiners (PARs)

For EDF, this principle appears to be, presently, as the better existing possibility, in term of technical and economical feasibility.

The only drawback effect seems to be the risk of initiation of a deflagration due to the oxygen/hydrogen exothermic chemical reaction.

The main technical question to be answer concerns demonstration of efficiency in severe accidents representative conditions.

For this, EDF has initiated or participate to the tests programmes described in § 4 to improve the present knowledge concerning PARs.

4 EDF PAR test programmes

EDF has initiated or participate to 3 PAR test programmes :

- KALI-H2,
- H2PAR,
- collaboration with EPRI.

4.1 KALI-H2

This programme, initiated by EDF, intends to study potential effect of Containment Spray System on PARs efficiency.

This programme had been performed, from 1993 to 1995 by CEA (French Energy Atomic Commission) using the so-called KALI-H2 facility located in Cadarache research centre (in south east of France, near Marseille).

The schematic flow diagram of the KALI-H2 facility is attached in figure 1.

Instrumentation is described in attached figure 2.

¹ All French NPP's are fitted with containment venting system using (external reactor building) sand bed filter and (internal reactor building) metallic prefilter.

Test vessel is a 15,6 m³ volume vertical axis cylinder (height # 4,6 m, internal diameter # 2,1 m). Design pressure is 12 bar (gauge) and design temperature 200°C.

The aim of test programme is to compare, in the same steam saturated condition, efficiency of PAR with and without spray actuated (when spray is actuated, pressure is kept at constant value during whole test duration by steam pressure regulation system).

Five atmosphere temperatures had been tested : 100, 110, 120, 130, and 140°C.

Hydrogen initial concentration is always the mass corresponding to 10% in dry conditions. This limitation is due to workers security reason (to absolutely exclude detonation risk in the facility).

Spray water includes chemical (soda and boric acid) as in the NPPs system.

PAR tested was a SIEMENS design complete device (catalytic material and housing) adapted to the scale of the facility.

As a general result, the PAR has functioned during the whole test programme without any catalytic sheets change.

4.2 H2PAR

The aim of this programme is to study PAR performance in presence of selected potential poisons (mainly aerosols, representative of core meltdown emission, produced by induction furnace).

This programme is managed by CEA/IPSN ² and EDF is associated by means of general agreement concerning R&D for NPPs safety.

The facility is located in CEA Cadarache.

First test is anticipated by the middle of this year.

This programme is described in reference /2/ presented at the same workshop.

4.3 Collaboration with EPRI

A collaboration programme between EDF and EPRI, to supplement the existing PAR test database is presently performed in CEA Cadarache using the KALI-H2 facility (see § 4.1).

The anticipated test matrix (same tests with SIEMENS design PAR and NIS design PAR) is attached in table 2.

More details are given in reference /3/ presented at the same workshop.

² IPSN : Nuclear Protection and Safety Institute

References

/1/ In-vessel hydrogen production assessment during severe accident sequences.

(J.L. Droulas, L. Nebois, B. de Magondeaux - EDF/SEPTEN - 1996 Annual meeting of ANS, 16 to 20 June 1996, Reno, USA)

/2/ The H2PAR facility for hydrogen research

(E. Studer, P. Rongier - CEA/IPSN - OECD/NEA CSNI workshop on the implementation of hydrogen mitigation techniques, 13 to 15 May 1996, Winnipeg, Canada)

/3/ Combustible gas control in advanced light water reactors.

(J. Hosler, G. Sliter - EPRI - OECD/NEA CSNI workshop on the implementation of hydrogen mitigation techniques, 13 to 15 May 1996, Winnipeg, Canada)

Type	CP1/2	P4	P'4	N4
Containment free volume (m ³)	49400	81400	70430	73050
H2 mass for 100% PAG (kg)	700	1000	1000	1060
Peak pressure for 75% PAG (bar abs.)	6,0	5,6	6,2	6,2
Peak pressure for 100% PAG (bar abs.)	7,5	7,0	7,7	7,7

Table 1 Hydrogen realistic deflagration peak pressure in French NPP's containments

Note

CP1/2 : First standardized French train (900MWe).

P4 : First 1300MWe standardized French train.

P'4 : Second 1300MWe standardized French train.

N4 : 1400MWe standardized French train.

TABLE 2
Final EPRI SIEMENS PAR Test Matrix
for KALI Test Program*

Test No.	Plant Type Simulated	Initial Gas Temp (°C)	Initial Gas Pressure (Bar)	Initial Gas Composition (Vol %)**					Remarks
				H ₂	Air	Steam	CO	Excess N ₂	
S-1	PWR	30	1.3	2	98	0	0	0	Low H ₂ conc (DBA)
S-2	PWR	30	1.3	2	98	0	0	0	Effect of wetness on start up ***
S-3	BWR	30	1.3	8	18	0	0	74	Effect of wetness on startup (low O ₂)***
S-4	BWR	30	1.3	8	18	0	0	74	Low O ₂ (BWR)
S-5	SIEM	112	2.9	5	45	50	0	0	Repeat of test previously conducted in Germany on PAR developmental designs
	NIS	100	2.0	3.6	46.4	50	0	0	
S-6	PWR	30	1.3	4	96	0	0	0	Effect of initial H ₂ concentration (DBA)
S-7	PWR	100	1.3	4	96	0	0	0	Gas temperature effect
S-8	PWR	30	4.0	4	96	0	0	0	Gas pressure effect
S-9	PWR	119	4.0	4	46	50	0	0	Steam Effect
S-10	BWR	63	1.3	8	18	18	0	56	BWR wetwell conditions
S-11	BWR	115	4.0	4	10	44	0	42	BWR drywell conditions
S-12	PWR	30	1.3	4	93	0	3	0	Inject CO when H ₂ reaches 2.5%
S-13	BWR	30	1.3	8	18	0	3	71	Inject CO when O ₂ reaches 3 %
S-14	PWR	30	1.3	10	90	0	0	0	Effect of initial high H ₂ concentration (Severe Accident)
S-15	PWR	30	1.3	4	96	0	0	0	Potential poisons from US cable fire
S-16	BWR	30	1.3	8	18	0	0	74	Potential poisons from US cable fire

* Matrix to be tested separately for NIS and Siemens PAR units

** After H₂ injection. "Excess Nitrogen" means in addition to the nitrogen in air.

*** Sprays (without chemicals) to be activated (30 °C) for 1 hour and then terminated prior to H₂ injection to ensure a fully wetted PAR

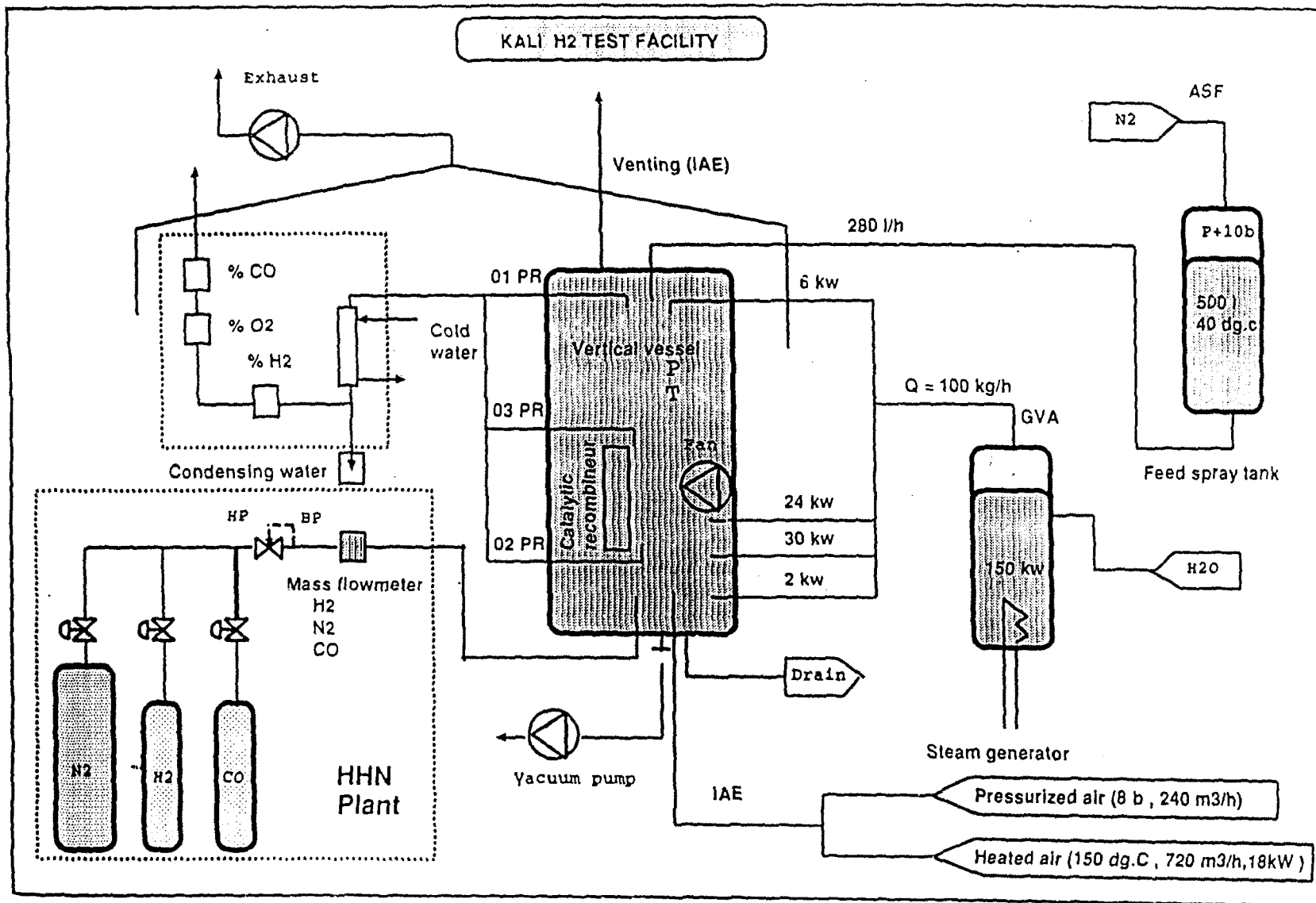


Figure 1

kaliniel

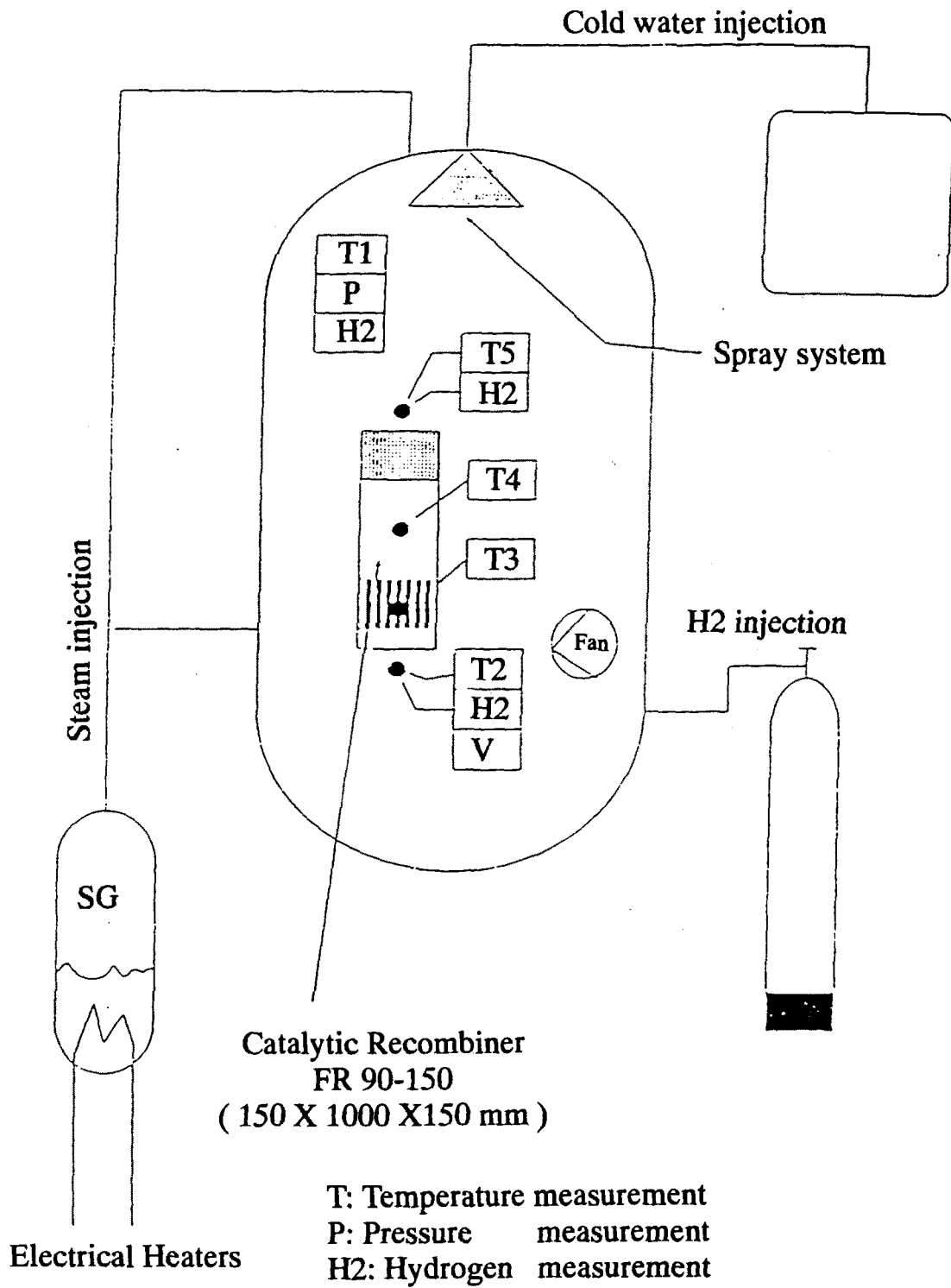


Figure 2: KALI-H2 instrumentation

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1.5 HYDROGEN MANAGEMENT STRATEGY FOR THE LOVIISA NPP

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Abstract

A new hydrogen management scheme has been developed for the Loviisa ice condenser containment as a part of a comprehensive severe accident management (SAM) strategy. The scheme is based on providing sufficient mixing of the containment atmosphere, effective energy removal from the containment, and controlled removal of hydrogen through passive catalytic recombination. The objective of the paper is to demonstrate how this hydrogen management scheme works for a range of relevant severe accident scenarios.

1 Introduction

The Loviisa nuclear power plant is a two-unit VVER-440 owned and operated by Imatran Voima Ltd (IVO). Figure 1 shows the geometry of the Loviisa ice condenser containment. The **lower compartment** (LC, sometimes also referred to as the **steam generator compartment**) is the release compartment in accidents. Its free volume is 6300 m³. The free volume of the **reactor coolant pump room** (also called **dead-ended compartment**) is 2600 m³. The volume of the **upper compartment** (UC) is 47 000 m³. The flow between the lower and upper compartment has to penetrate the **ice condensers** (IC), and there are no recirculation fans. There are two separate ice condenser sections containing a total of at least 835 000 kg ice in normal operation. The free volume of the ice condensers filled with ice is 3500 m³, with empty ice beds it is 4500 m³. The total free volume of the containment (excluding the dead-ended compartment) is thus 58 000 m³. The design pressure is 1.7 bar(a) and the failure pressure has been estimated to be 3.25 bar(a). Long-term residual heat removal is performed by externally spraying the steel shell of the containment dome.

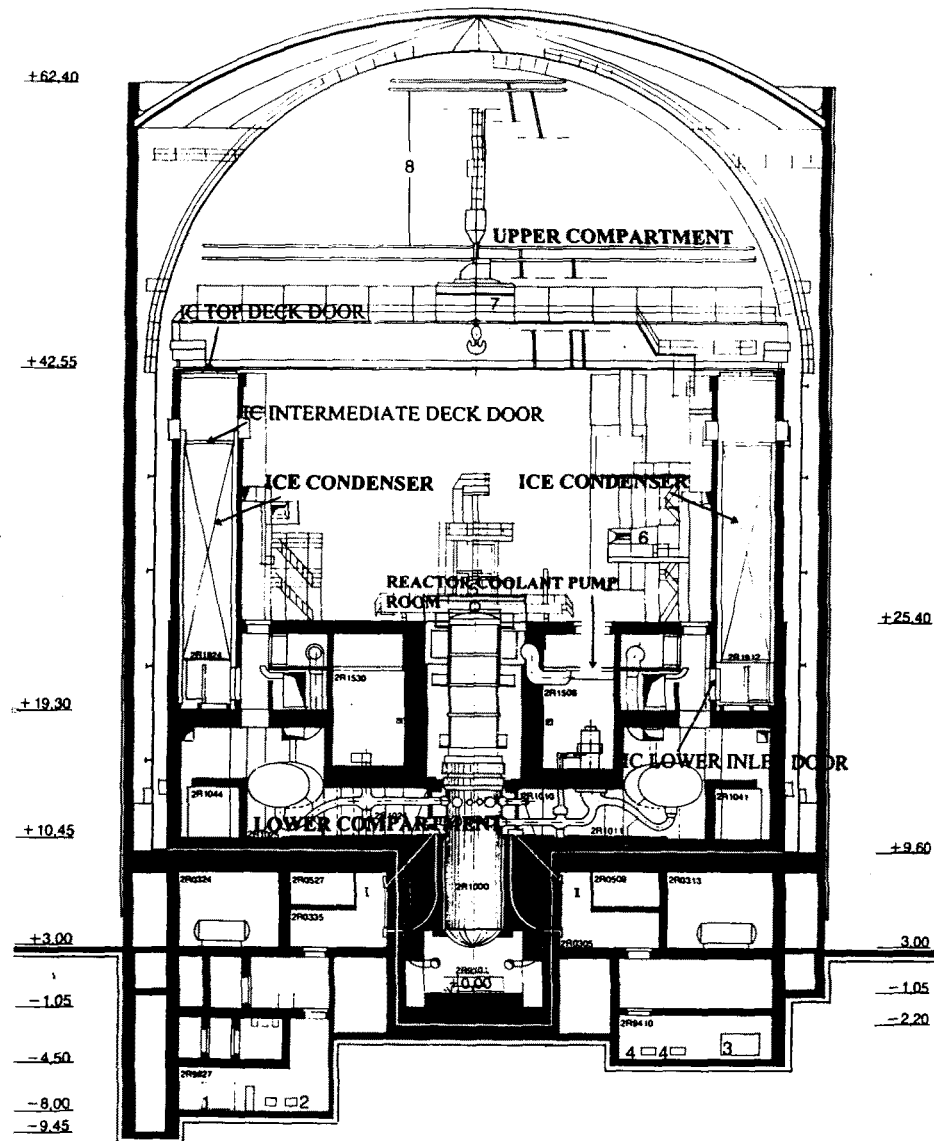


Figure 1 Ice condenser containment of the Loviisa NPP

The ice condenser **lower inlet, intermediate deck, and top deck doors** are important flow restrictions in the containment. The vertical lower inlet doors open into the ice condensers, when the pressure difference across the doors increases due to gas and energy injection into the lower compartment. They have springs attached to them to create a closing torque, when the lower compartment overpressure has been relieved. The lower inlet doors (8 door pairs per IC section) quite efficiently prevent a return flow of cold air to the lower compartment. The horizontal intermediate deck doors (72 doors per IC section) are heavy structures that remain in place except during the initial blowdown period of a large break loss-of-coolant accident. The top deck doors are large non-rigid horizontal flaps (18 flaps per IC section). There are narrow **bypasses** at the intermediate and top decks for replacement of air previously being pushed out of the ice bed into the upper compartment by

steam entering from the lower compartment. When the steam condenses in the ice bed, a subpressure is created in the ice condensers, and air is sucked back through the bypasses.

Glow plugs powered from the emergency diesel generators were installed in the Loviisa containments in 1982, 74 plugs in Loviisa 1 and 66 plugs in Loviisa 2. There are glow plugs in all lower compartment rooms, above and below the ice beds in the ice condensers, and in the upper compartment. The emergency operating procedures instruct the operators to switch on the glow plugs at an early stage of an accident. Certain issues concerning the adequacy of the current hydrogen combustion mitigation approach at the Loviisa NPP can be raised. It is uncertain where and at what hydrogen concentrations the hydrogen would burn as a result of ignition by the glow plug system, and it is not well understood how energy generated in hydrogen combustion would be dissipated in the containment. An additional concern is that the glow plugs and their cabling were not qualified for ice bed conditions.

The hydrogen management options feasible to the Loviisa containments were discussed in [1]. There it was concluded that the most promising strategy would be based on **providing sufficient mixing of the containment atmosphere, effective energy removal from the containment, and controlled removal of hydrogen through passive catalytic recombination (or a combination of passive catalytic recombination and ignition)**. The central SAM measure towards ensuring this type of behavior is to open all ice condenser doors - this is done as an operator procedure as part of a comprehensive severe accident management scheme [8]. With the doors open a **global** (containment-wide) **loop flow** would set in due to naturally occurring gravity head imbalances, ensuring efficient mixing, especially during hydrogen release periods, as well as allowing ice to become an effective heat sink for the energy released in the recombination.

The purpose of this paper is to explain quantitatively how the proposed new hydrogen management scheme would work at the Loviisa NPP. Key physical mechanisms and phenomena that must be well understood are the global convective loop flow in the containment, and mixing/stratification of the upper compartment. These will be described in the paper. We will explain how the convective loop flow can be modelled, and how the models can be validated. We will apply the models to demonstrate how the proposed strategy works in relevant severe accident situations.

2 The hydrogen management scheme

The hydrogen management strategy we propose is based on four logical steps. First we demonstrate that a **global convective loop flow** will take place in the containment during hydrogen release, if the ice condenser doors are forced open. Convective flow patterns in the containment are discussed in section 3.1. The loop flow is driven by the difference in hydrostatic pressure between the ice condensers. We can quantify volumetric flow rates and identify the most significant parameters affecting the loop flow rate, see section 3.2. Based on the basic loop flow

pattern, we can draw conclusions on important issues such as upper compartment mixing behavior (section 3.3).

Second, we define the **maximum hydrogen molar fraction** (in combination with steam molar fraction) **that can be tolerated** in the containment, see chapter 4. There will be a global criterion, for which the adiabatic isochoric complete combustion (AICC) pressure approaches the containment failure pressure. There will also be a local criterion, since flame acceleration and transition to detonation should be avoided anywhere in the containment.

Third, an assessment of the **hydrogen generation** rate in the core is carried out, see chapter 5. An increased hydrogen injection rate will always accelerate the convective loop flow rate, since it will temporarily create a greater imbalance between ice condenser gravity heads. This is true for any ice bed configuration. This means that faster hydrogen release rates are compensated by enhanced mixing.

Fourth, given the basic results from the first and third steps, the **passive catalytic recombiner system** can be dimensioned (chapter 6) to fulfil our criteria from the second step for the complete and appropriate envelope of scenarios and conditions.

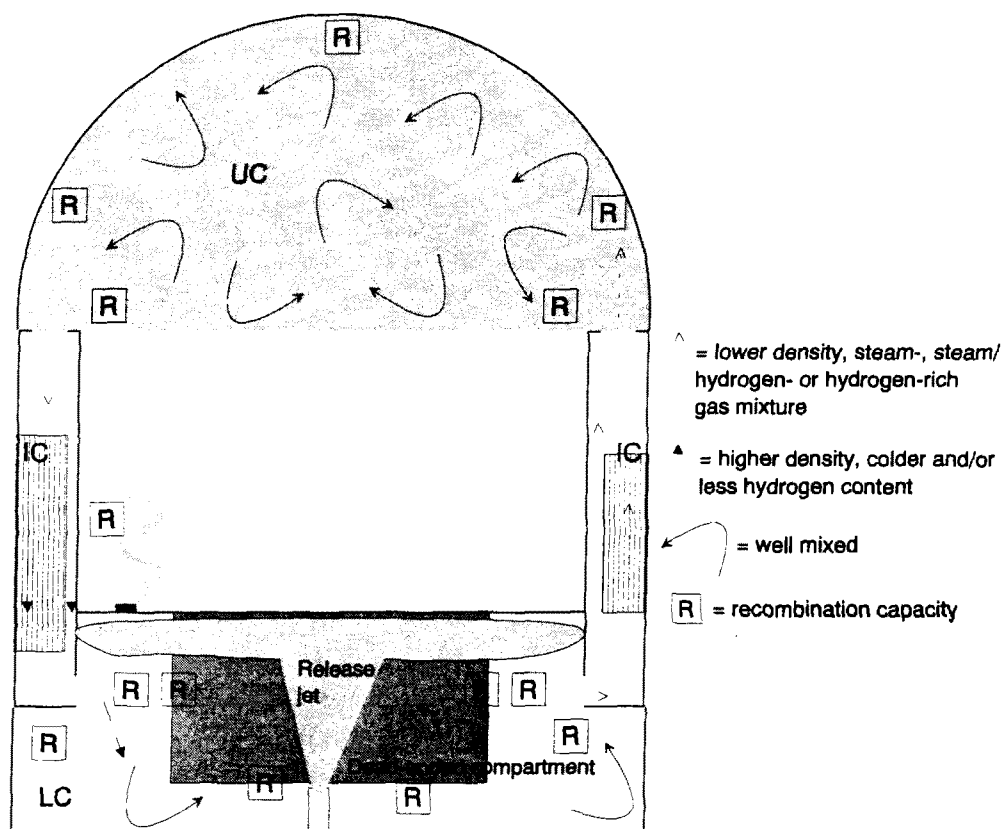


Figure 2 Hydrogen management during a severe accident

Figure 2 is an overall illustration of the proposed hydrogen management concept in the Loviisa containment. In this case, hydrogen or a steam-hydrogen mixture is being released through the quench tank rupture disk in the lower compartment. The nature of the hydrogen source will be described in chapter 5. The quench tank is situated close to the containment wall between the ice condenser sections. Figure 2 shows the convective loop flow in the containment - as a consequence of forcing open ice condenser doors - with upflow of light gas along one IC and downflow of a heavier gas mixture along the other one. The merging of the hydrogen-rich gas stream from the release with the loop flow increases the driving density difference between the ice condensers, but the large volume of the system and efficient mixing ensure that hydrogen concentrations increase slowly and uniformly. The catalytic recombiner system is dimensioned and spatially allocated so as to maintain peak possible concentration levels within the tolerable limits, even allowing for 100% oxidation of the active zirconium in the core.

The small volume above the ice condenser lower inlet door in the LC tends to stratify. In order to efficiently break up this stratification, passive catalytic recombiners are placed to inject into this layer. Otherwise, the lower compartment and the upper compartment dome region are virtually well-mixed (with the obvious exception of the actual release jet in the LC). The upper compartment is stratified in the sense that the region below the IC outlet level does not participate in the mixing process. Concentrations of hydrogen in this region increase very slowly, due to leakage paths (called "goose-necks") on the main operating deck. This is also indicated in Fig. 2. Therefore some recombination capacity should also be assigned to the lower part of the upper compartment.

3 Convective flow behavior in the containment

3.1 Convective flow modes

We foresee two main convective flow patterns when the ice condenser doors are forced open in the Loviisa containment in the post-blowdown accident phase, as depicted in Figures 3 and 4.

While the ice beds are still substantial (only little ice has melted), a pure steam injection can result in the flow behavior of Fig. 3. The convective flow is driven by the density differences **within** each ice condenser. This flow pattern is expected (and seen experimentally) to be very symmetrical. There is upflow along the inner wall of each ice condenser, where a free flow channel is created little by little, and downflow of cold air in the massive ice bed. There is a pronounced countercurrent flow through the forced-open lower inlet doors. The upflow arrows reaching halfway through the ice beds in Fig. 2 indicate that all the steam added to the lower compartment can be absorbed by the ice beds without heating up the upper plenums of the ice condensers (located between top deck and intermediate deck doors). Since there is no injection of non-condensable gases, and the injected steam is dissipated before penetrating the ice bed, **there is no flow through the ice condenser top deck**. Therefore, the upper compartment is not at all part of this flow path.

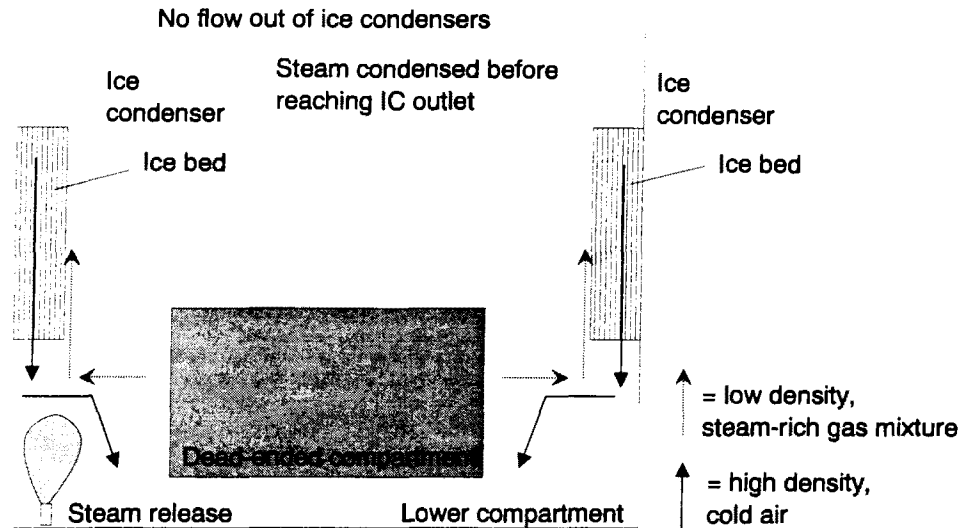


Figure 3 Convective flow before involving the upper compartment

However, when a light gas mixture reaches the ice condenser upper plenum (see upper part of Fig. 4), a flow pattern transition is expected, and experimentally seen, to take place. The light gas mixture is either a steam-air mixture, if the channelling of the ice bed is sufficient to allow steam penetration, a steam-air-hydrogen mixture, or theoretically even a cold air-hydrogen mixture. When the gas mixture entering the IC upper plenum from the ice region is positively buoyant, it will rise and enter the upper compartment through the expanded flow paths of the top deck. Now there is a positive net flow into the upper compartment, which has to be compensated by an outflow of gas from the upper compartment somewhere else. Since the upper compartment is connected with the rest of the containment rooms only by the ice condensers, a downflow will be (and is experimentally seen to be) initiated through the other ice condenser.

The conditions at this point tend to create imbalance between the ice condensers, even if we happened to have a fairly symmetrical ice configuration. As a consequence of releasing steam and hydrogen into the **lower compartment**, and running the gases through ice before reaching the upper compartment where mixing is vigorous, the steam fraction, hydrogen fraction and temperature are significantly lower in the upper compartment than in the lower compartment. When this higher-density gas is sucked down into the other ice bed, the temperature and steam fraction decreases even more. Therefore we now inevitably have an ice condenser containing higher density gas (due both to a lower steam and hydrogen molar fraction) than the other one. Since the ice condenser structure height is about 23 m, even a small density difference causes a significant difference in gravity heads. The imbalance drives the global convective loop flow which can always be maintained, unless the upper compartment is heated much more than the lower compartment (has to be kept in mind when determining recombiner locations) or the release of light gas takes place in the upper compartment (not possible in Loviisa severe accident scenarios).

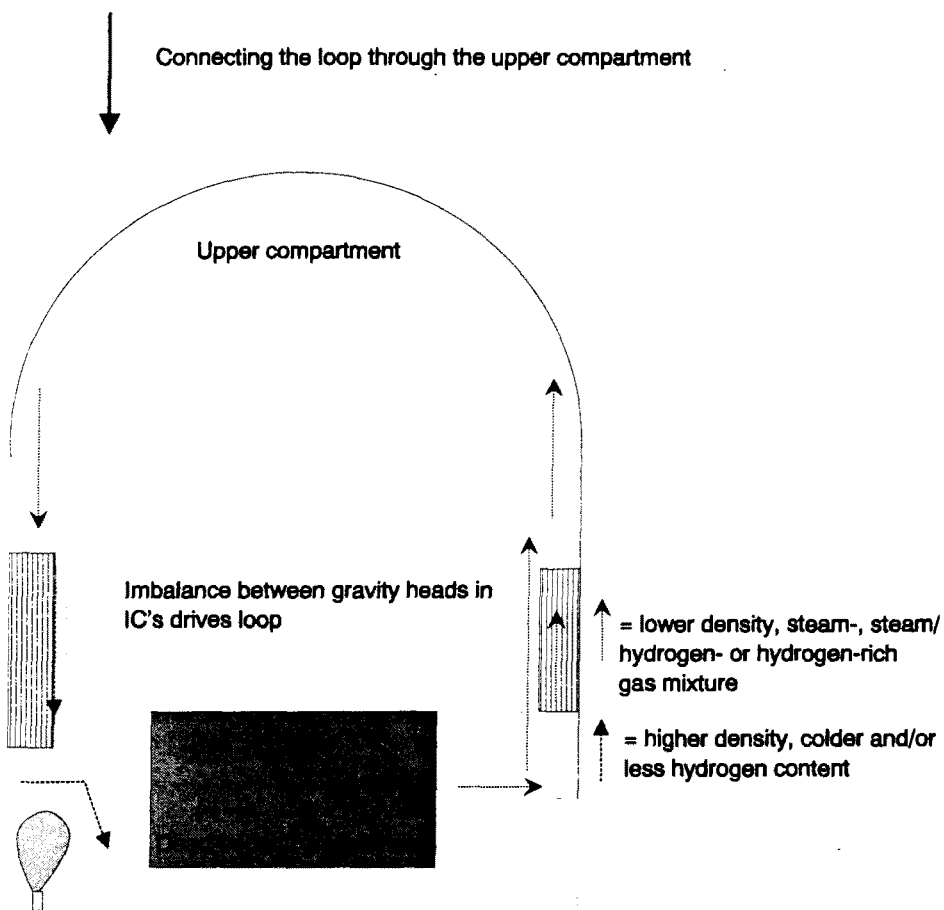
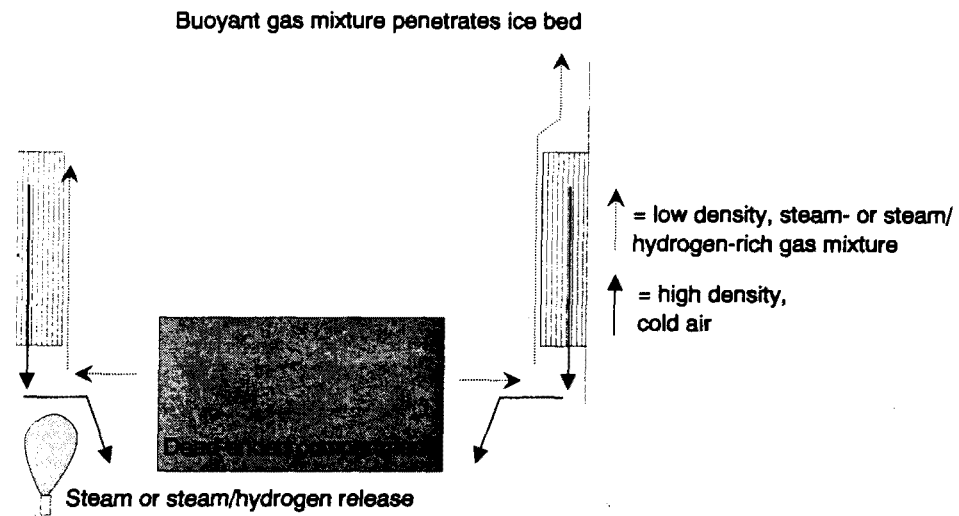


Figure 4 Global convective loop flow

When determining which convective flow mode is to be expected at the time for hydrogen release, understanding ice melting behavior is crucial. The general conclusion of [2] is that during violent releases of steam into the containment, as typically during the blowdown phase of a LOCA, the ice condenser lower inlet doors and the ice bed are "flooded" by the steam flow. This means there is no countercurrent flow through the door openings and no downflow of cold air in the ice bed. Heat transfer to the ice is very efficient, and the ice bed melts uniformly from the bottom upwards. Contrary to that case, a slower post-blowdown phase steam release will channel through the ice bed, after which the general behavior depends on whether the intermediate deck doors are open or not. Since large amounts of energy has to be released into the containment in Loviisa before the core has a chance to uncover, **we do expect to have steam channelled through the ice bed at the time of core uncover.** This is the point in time when the SAM measure of forcing open the ice condenser doors is to be performed. Since buoyant gas has already reached the ice condenser upper plenum, the loop flow through the upper compartment can be initiated.

3.2 Loop flow rates

The loop flow in the containment can be modelled by demanding that the difference in hydrostatic pressure between the ice condenser sections is consumed as pressure losses due to flow friction along the loop flow path. The by far most important locations of pressure loss due to flow friction in the containment are the **lower inlet doors, ice beds, intermediate deck and top decks**. These locations can be identified as the only points of real flow constriction in the containment. Demanding that the difference in hydrostatic pressure between the ice condensers is consumed at these locations we arrive at

$$\Delta \rho g \Delta H = \frac{1}{2} (\zeta_{LI,up} \rho_{LI,up} u_{LI,up}^2 + \zeta_{IB,up} \rho_{IB,up} u_{IB,up}^2 + \zeta_{ID,up} \rho_{ID,up} u_{ID,up}^2 + \zeta_{TD,up} \rho_{TD,up} u_{TD,up}^2 + \zeta_{TD,down} \rho_{TD,down} u_{TD,down}^2 + \zeta_{ID,down} \rho_{ID,down} u_{ID,down}^2 + \zeta_{IB,down} \rho_{IB,down} u_{IB,down}^2 + \zeta_{LI,down} \rho_{LI,down} u_{LI,down}^2) \quad (1)$$

where the subscript *LI* refers to the quantities at the lower inlet doors, *IB* at the ice bed, *ID* at the intermediate deck, and *TD* at the top deck. *Up* refers to the ice condenser where upflow takes place, and *down* to the downflow ice condenser. The quantity $\Delta \rho$ (kg/m³) represents the average density difference between the ice condenser over a height ΔH (m), u denotes flow velocities (m/s), ρ gas densities (kg/m³), and ζ flow friction parameters.

We can get reasonably accurate estimates of the volumetric loop flow rate in different conditions applying equation (1) even by hand calculations. Some iterations have to be performed, since the flow friction coefficients are functions of the Reynolds number, and thus of the flow velocity. We will here apply equation (1) to Loviisa in order to demonstrate what order of magnitude of volumetric loop flow rates to expect in the containment due to differences in hydrogen molar

fractions between the ice condensers. We will demonstrate how the approach works for a VICTORIA experiment.

In order to keep track of more complex transients and interactions, we developed a computer model for calculating containment conditions and resulting loop flow rates, based on the concepts described above. The model is called "Virta", and it basically solves equation (1) updating flow friction parameters and containment conditions as functions of time. The flow friction parameters are modelled as described in this paper. The model will be applied for a demonstration of recombiner system dimensioning at Loviisa in chapter 6.

The left hand side of equation (1) represents hydrostatic pressure difference driving the flow. It is determined by the scenario characteristics, the most important ones being:

- **Asymmetry of ice bed**
- **Steam release rate**
- **Hydrogen release rate**
- **Heat transfer to containment structures and sump water.**

The right hand side of (1) deals with the pressure losses of the flow in the containment, and can be reduced to modelling/calculating the flow friction parameters ζ . Next we will outline the approach to flow friction parameter modelling, which is based on [3]. The flow friction parameter at location i has the following physical meaning:

$$\zeta_i = \frac{\Delta p_{loss,i}}{\rho_i u_i^2 / 2} , \quad (2)$$

where $\Delta p_{loss,i}$ denotes the pressure loss due to flow friction at location i .

At the **ice condenser lower inlet doors** the flow pattern is depicted as in Fig. 5. On the so called hot side there is an inflow into the ice condenser, as on the left in Fig. 5. On the cold side there is an outflow from the ice condenser into the lower compartment, as on the right in Fig. 5. The friction coefficients ζ for different opening angles for inflow and outflow, respectively, are listed in [3].

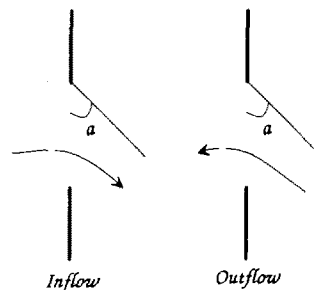


Figure 5 Flow through forced open ice condenser lower inlet doors, with opening angle a

The pressure losses due to flow friction at the **intermediate deck and top deck constrictions** can be modelled as orifices in the flow path, Fig.6. The flow passes a constriction with a flow cross section of A_0 . Before and after the constriction there are flow cross sections of A_1 and A_2 , respectively. Even though A_1 is here depicted to be larger than A_2 , this does not have to be the case in the application.

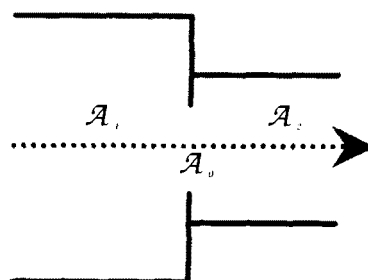


Figure 6 Orifice in flow path

As explained in section 3.1, the steam tends to channel through the ice beds along the inner wall of the ice condenser structure, for a steam release in the post-blowdown phase of the accident. Therefore the **ice condenser** is modelled to have **a free flow channel along the inner wall and an ice bed that consists of cylindrical ice rods**. The general expression for the friction coefficient as a function of free channel or ice bed height l is

$$\zeta_i = \lambda k_i \frac{l_i}{D_{h,i}}, \quad (3)$$

where λ is the friction coefficient for a circular tube, k_i a correction coefficient for the actual geometry, l_i length of flow path, and $D_{h,i}$ the hydraulic diameter of the flow channel in question.

Table I Flow friction parameters at Loviisa

Location	ζ_i	U_i^2/U_{LI}^2	Portion of total pressure loss in loop
LI, up	3.5	1	15.5%
LI, down	3.5	1	15.5%
ID, up	1.5	1.515^2	15.2%
ID, down	1.5	1.515^2	15.2%
TD, up	2.08	0.928^2	7.9%
TD, down	1.35	0.928^2	5.1%
IB, up	2.1	1.169^2	12.8%
IB, down	2.1	1.169^2	12.8%

Let us assume we have a symmetrical ice configuration in Loviisa with 50% of the original ice left in the ice condensers. This assumption is consistent with the ice melting rate findings and the time for onset of hydrogen generation in the core. The ice bed height, thus, is 7.3 m and the ice rod diameter is assumed to be the original, unmelted 0.32 m. The ice bed flow friction factor decreases quickly with narrowing ice rods. The lower inlet doors are assumed to be forced open to their maximum opening angle of 50°, and half of the top deck and intermediate deck doors are forced fully open. The resulting flow friction parameters at the different pressure loss points are shown in Table I.

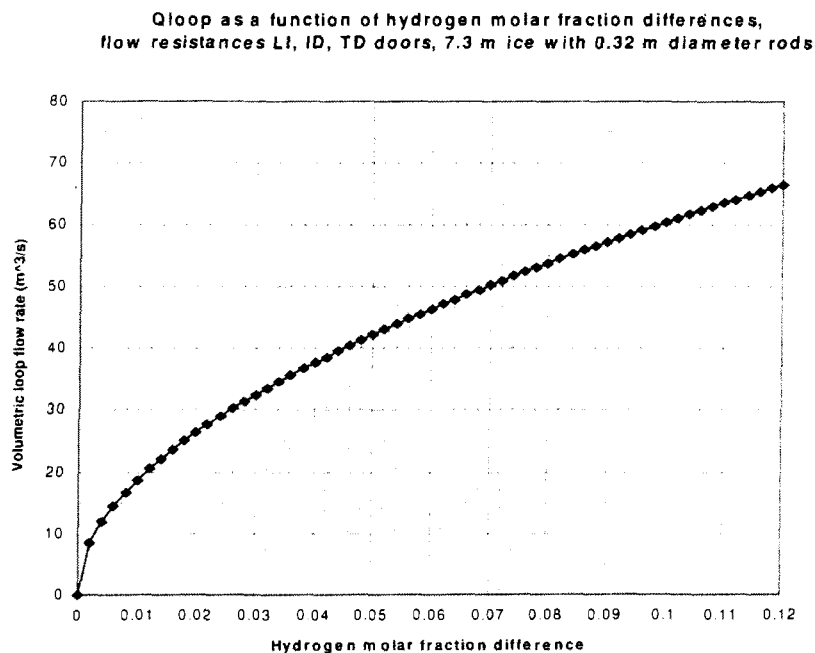


Figure 7 Volumetric loop flow rate as function hydrogen concentration difference

We assume that the entire containment is completely isothermal (conservative with respect to hydrogen mitigation, since temperature differences between the ice condensers only accelerate the flow) and inject pure hydrogen. Figure 7 shows the volumetric loop flow rates that would result as a consequence of the difference in hydrogen molar fraction along the ice condensers. This imbalance would be expected to occur in reality due to the fact that there is a very large mixing volume for the flow, the upper compartment dome region, between the ice condensers. Flow rates were calculated with equation (1), with the flow friction coefficients of Table I. The gravity head difference was calculated as resulting from the given difference in hydrogen molar fraction - but identical temperatures and steam molar fractions - along the height of the ice condenser structure above the lower inlet doors (= 21 m). Figure 7 shows that a hydrogen molar

fraction difference of 4%(units) drives a loop flow of $38 \text{ m}^3/\text{s}$, and a 9% imbalance results in a volumetric loop flow rate of $57 \text{ m}^3/\text{s}$.

Figure 7 explains how an increased hydrogen release rate acts to accelerate the convective loop flow. Let us assume the volumetric flow rate is $30 \text{ m}^3/\text{s}$ at the onset of hydrogen generation, and hydrogen gas is released with $0.1 \text{ kg/s} = 0.05 \text{ kmol/s}$. At typical lower compartment conditions in Loviisa at this point ($T=323\text{K}$, $p=1.1 \text{ bar}$), the injected volumetric flow rate is $1.22 \text{ m}^3/\text{s}$. Since the light gas cannot be trapped to any significant extent in the lower compartment, a hydrogen molar fraction of approximately 3.9% is quickly created in the upflow ice condenser, having a fairly small free volume. It will take several minutes until the upper compartment dome region will increase its hydrogen molar fraction to even 1%. Therefore there will exist an imbalance between the ice condensers of about 4%(units) hydrogen molar fraction. Figure 7 then shows that due to this effect alone - not considering obviously existing temperature differences - the volumetric loop flow rate should accelerate to almost $40 \text{ m}^3/\text{s}$. The increased volumetric flow rate again will serve to improve mixing and reduce the imbalance.

Table II Flow friction parameters at VICTORIA, experiment 44

Location	ζ_i	U_i^2/U_{Li}^2	Portion of total pressure loss in loop
LI, up	3.5	1	23.1%
LI, down	3.5	1	23.1%
ID, up	0.36	0.671^2	1.1%
ID, down	0.36	0.671^2	1.1%
TD, up	1.73	1.26^2	18.2%
TD, down	1.34	1.26^2	14.1%
50% IB, up	2.59	0.75^2	9.65%
50% IB, down	2.59	0.75^2	9.65%
25% IB, up	1.3	0.75^2	5.4%
25% IB, down	1.3	0.75^2	5.4%

Since the VICTORIA experimental facility is geometrically similar to Loviisa, we have the same dominating pressure loss locations as in the containment. However, the resulting values are different. Flow friction parameter modelling can be carried out exactly as for Loviisa. Table II shows the flow friction parameters for an experiment where the lower inlet doors were forced open to 50° , all intermediate deck door flaps were removed, and the top deck flow area was restricted to $1/3$ of its maximum value. Half of the ice is assumed to be left in the ice condensers in the beginning of the experiment.

VICTORIA experiment 44 was an example of a case where helium molar fraction differences drive the loop flow during the helium injection phase. The experimental arrangement was described in [1]. The experiment was started with 50% of the ice left in each ice condenser and a high steam injection rate at 10 g/s . At 25 minutes, steam injection was interrupted while helium

was injected during 30 minutes. Helium molar fraction measurement data is shown in Fig. 8. Volumetric loop flow rate data, with flow velocities measured on top of the restricted IC top decks with a LDA, are shown in Fig. 9. Since there is no steam injection during the helium injection phase between 25 and 55 minutes, and the vessel temperatures are very uniform, helium molar fraction differences in the ice condensers mainly drive the loop flow. Therefore, the approach that produced Fig. 7 can be tested for this experiment during the helium injection phase.

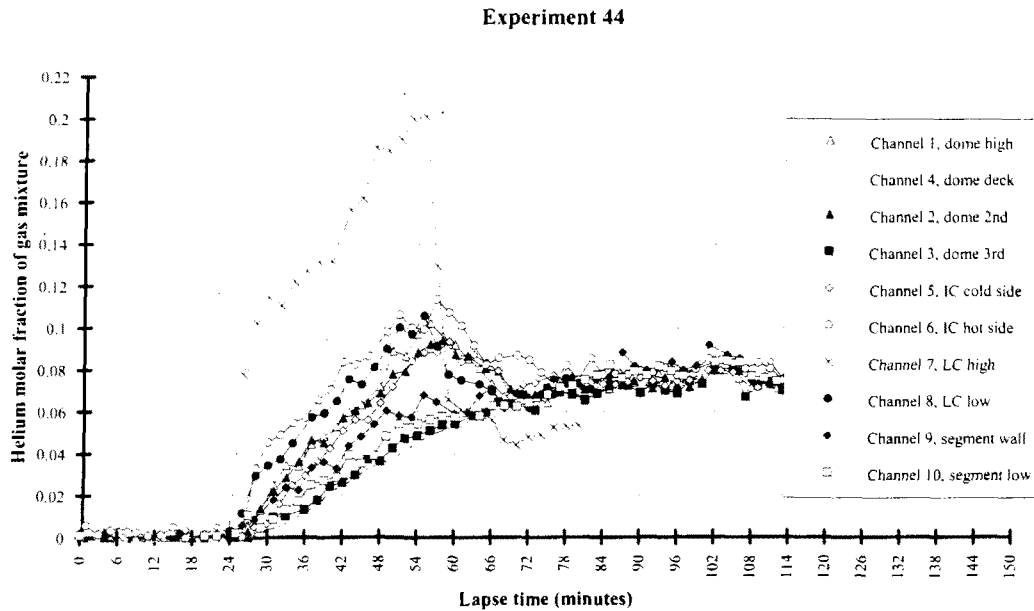


Figure 8 Helium molar fractions in experiment 44 [1]

We assume that the helium molar fraction differences are determined by the two merging gas streams - the actual loop flow and the injected helium gas - in the upflow ice condenser. The volumetric loop flow rate can be seen to be a very uniform $0.035 \text{ m}^3/\text{s}$ (Fig. 9) during most of the helium injection phase. At 42 minutes, assuming [4] a pressure of 1.073 bar and a UC temperature of 30°C , this can be converted to a $\dot{n}_{\text{loop}} = 0.00149 \text{ kmol/s}$. The molar helium injection rate is $\dot{n}_{\text{He}} = 0.0000357 \text{ kmol/s}$ (since the helium injection rate is 0.8 nl/s at 273 K , 1.013 bar).

Merging these two streams will create a helium molar fraction difference between the downflow IC and the upflow IC of approximately 2.3%. This imbalance is quite close to what can be seen to have been the case in the experiment, see Fig. 8, channels 5 and 6 between 25 and 55 minutes. At 42 minutes, it will result in $\Delta\rho/\rho_{\text{light}} = 0.0213$. Let us assume now that this density difference in the ICs above the lower inlet doors ($H = 1.25 \text{ m}$) drives the loop flow. Equation (1) can be solved to render $u_{\text{LI,down}} = 0.196 \text{ m/s}$, and since the LI flow area is 0.184 m^2 for one IC section we obtain $Q_{\text{loop}} = 0.036 \text{ m}^3/\text{s}$.

The resulting loop flow rate is very close to what is indicated in Fig. 9 (and to the value assumed above in the hand calculation). The flow friction parameter was based on the assumption that half of the original 50%/50% ice bed had melted at 42 min, which is consistent with energy injection and complete ice depletion. The ice bed flow friction parameter is the most uncertain parameter in the calculation above. However, as can be deduced from Table II, the effect of variation in this parameter is negligible.

Clearly, the imbalance of that drives the loop flow depends very much on the effective "capacitance" (mixing volume) of the upper compartment. This is addressed next.

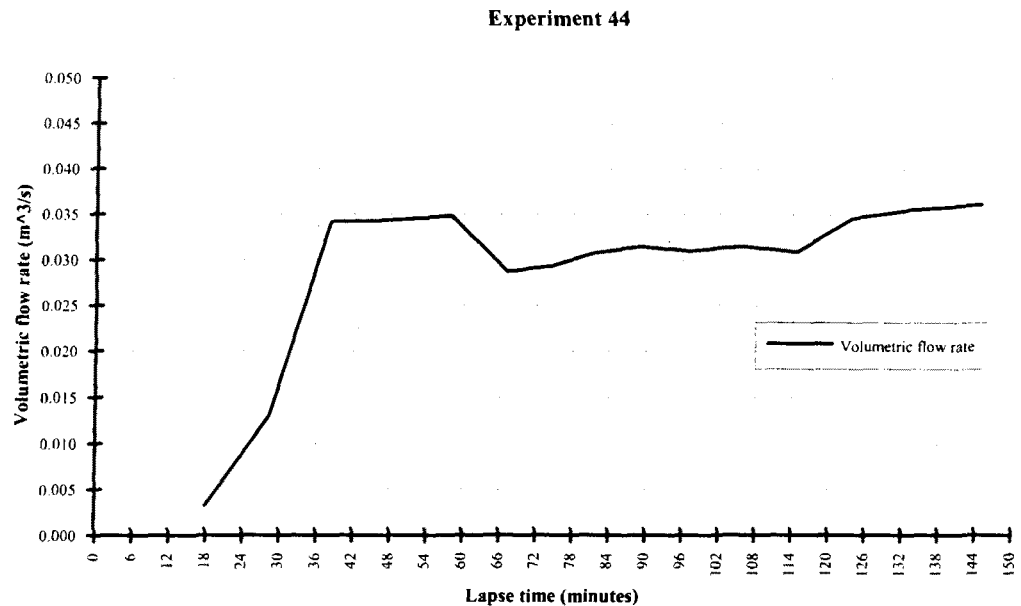


Figure 9 Volumetric flow rate data of experiment 44

3.3 Upper compartment mixing/stratification

From basic considerations (see for example [5,6]) we expect that the upper compartment will be stratified at the level of the IC top deck doors, as illustrated in Figure 2 - with the upper portion being well-mixed and the lower portion remaining relatively isolated from this mixing process. The mechanism for this behavior is provided by **turbulent plume mixing and entrainment**, as explained next.

A jet or plume being injected into a compartment can be characterized by its injection Froude (Fr) and Reynolds (Re) numbers:

$$Fr = \sqrt{\frac{\rho_0 \cdot U_0^2}{g \cdot D_e \cdot (\rho_a - \rho_0)}} \quad (4)$$

where ρ_0 = jet or plume inlet density (kg/m^3), ρ_a = ambient density (kg/m^3), U_0 = injection velocity (m/s), D_e = jet or plume initial diameter (m), g = gravitational acceleration (m/s^2). Fr expresses the ratio of inertia and buoyancy forces in the jet. The Reynolds number is defined as

$$Re = \frac{U_0 \cdot D_e}{\nu}, \quad (5)$$

where ν = kinematic viscosity of the jet gas mixture (m^2/s). The Reynolds number is the ratio of inertia and viscous forces in the jet. When a certain critical Reynolds number is exceeded, a transition from laminar to turbulent flow takes place in a jet. During the global convective loop phase, gases can be shown to enter the upper compartment from the upflow ice condenser in the form of turbulent plumes, since Reynold numbers are very high on this scale.

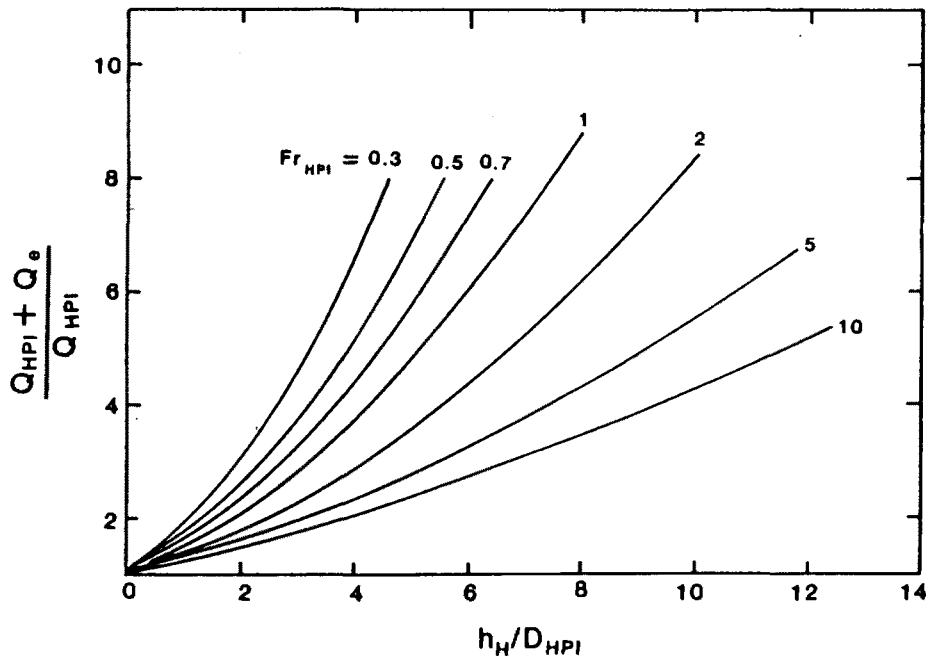


Figure 10 Prediction of total flow in axisymmetric plumes/jets [5].

The Froude number of such plumes entering the upper compartment is the important parameter. **Plume mixing, and hence decay, depends only on the injection Froude number and on the distance from the injection point.** The mixing behavior of turbulent plumes at different Froude number ranges has been studied in great detail in the context of management of pressurized thermal shocks (PTS) of the reactor pressure vessel, [5, 6]. The results for total plume flow at a dimensionless distance L/D_{inj} from the injection point, with Fr number as the parameter, are shown in Figure 10. The model applied in the calculation of the curves in Fig. 10 utilizes the standard equation of natural convection boundary layer flows with a vertically oriented buoy-

ancy force and a $k - \varepsilon - \overline{T''^2}$ turbulence model. It has been extensively validated against experimental turbulent jet and plume data. Although the present case involves restricted plumes by the containment wall along a rather extended arc (forming what might be called planar semi-plumes) these results are applicable for the basic purposes employed here.

Q_{HPI} in Fig. 10 corresponds to the loop volumetric flow rate in our case. Q_e is the entrained volumetric flow rate at distance L/D_{inj} from the top deck in the upper compartment (h_H/D_{HPI} in the figure). The quantity on the y-axis thus determines how many times larger the total volumetric flow (injected flow + entrained flow) is than the flow originally injected into the upper compartment. Figure 10 shows that entrained flow is an order of magnitude greater than injected flow, within a relatively short distance, and that the situation improves as the Fr number decreases. The reason is that weaker plumes become increasingly more unstable and, in fact, for Fr numbers less than 1 they cannot even exist unless the flow is stabilized at the inlet.

Table III Froude numbers at IC top deck

H ₂ release rate (kg/s)	Loop flow rate (m ³ /s)	H ₂ molar fraction diff. between ice condensers	UC injection Fr
0.1	35	0.035	1.0
0.15	40	0.046	1.0
0.2	45	0.054	1.0
0.3	51	0.072	1.0

Table III shows a range of typical for our case hydrogen release rates, volumetric loop flow rates, hydrogen molar fraction differences, and associated injection Froude numbers into the upper compartment pertinent to the Loviisa case. The loop flow rates were determined to be consistent with the assumed hydrogen release rates and hydrogen molar fraction imbalance in the sense that the emerging H₂ molar fraction difference (between the ice condensers) drives the given flow. The selected loop flow values correspond to the situation described in section 3.2 and Fig. 7. Hydrogen is assumed to be released to a lower compartment with a temperature of 323 K and a pressure of 1.1 bar. The top deck flow area is 48.5 m² (half of the top deck doors are forced open), and the hydraulic diameter at UC injection is 1.6 m. The inlet flow velocity in equation (4) is determined by the total loop flow rate and the top deck flow path area. The quantity $(\rho_a - \rho_0)/\rho_0$ in equation (4) is calculated with the assumption that the hydrogen molar fraction is given in the third column in Table III, and that of the heavy surrounding gas mixture (originally present in the upper compartment) is 0. The steam molar fraction is assumed to be 0.11, and the temperature 323 K. All Fr numbers turn out to be ≈ 1 .

Table IV shows how these low Froude number plumes can be expected to affect the upper compartment. Since the IC top deck level is at +42.55 m and the centerline of a vertical plume would hit the dome at approximately +55 m, we look at mixing at the point where $L/D_{inj} = 12 \text{ m} / 1.6 \text{ m} = 7.5$. The last column in Table IV shows how long it would take to recirculate the dome

region of the UC ($= 27\,000\text{ m}^3$) with the volumetric flow rate corresponding to the total flow, it-being the injected loop flow rate (Q_{loop}) plus the entrained flow rate (Q_e).

Table IV UC dome region mixing

Loop flow rate Table III (m^3/s)	UC injection Fr Table III	L/D_{inj}	$Q_e + Q_{loop}$, Fig. 10 Q_{loop}	Dome region re- circulation time (s)
35	1.0	7.5	8	96
40	1.0	7.5	8	84
45	1.0	7.5	8	75
51	1.0	7.5	8	66

The dome region recirculation times vary between 66 and 96 s. These are very short times compared to the time constant for the hydrogen concentration transient. Therefore, the upper compartment dome region will respond very closely as a well-mixed compartment above the plume injection point (= the IC top deck). This behavior has been experimentally verified in the VICTORIA facility, as described in [7].

4 Maximum tolerable hydrogen concentrations

The hydrogen management scheme should fulfil two basic requirement for the Loviisa containment. First, locally we do not want to risk developing accelerated flames (and e.g. jet ignition of subcompartments). Second, the global distribution of hydrogen should be such that the adiabatic isochoric complete combustion pressure cannot exceed 3.25 bar(a), it being the calculated ultimate failure pressure of the containment.

In order to avoid the accelerated flame mode, **the hydrogen molar fraction should be kept below 9% locally**. This is considered quite adequate for meeting the first requirement under the steam molar fractions of 25-40% at the time for maximum hydrogen molar fractions.

Regarding the second requirement, concentrations of up to 8% can be tolerated (AICC pressure approaching the failure limit) in the well-mixed volume ($38\,000\text{ m}^3$) participating in the loop flow. But as can be deduced readily from the above, such a situation would have already defeated the local limit of 9%; this is taken, therefore, as the single needed limiting criterion for sizing the recombiner capacity.

5 The hydrogen source

The adopted SAM strategy [8] at the plant defines the hydrogen source in many significant respects. The most important measures are

- **In-vessel retention of molten corium** [9]. This SAM measure and implemented plant modifications ensure that reactor pressure vessel integrity will be maintained by external cooling of the vessel wall. As a consequence we do not need to consider hydrogen source from core-concrete interaction or ex-vessel FCIs.
- **Intentional depressurization of the primary circuit with depressurization valves.** This SAM measure ensures that high-pressure core meltdown sequences can be prevented. Therefore core degradation does not proceed in surplus steam, and hydrogen will not be stored to any significant extent in the primary circuit. The hydrogen release to the containment will be very closely related to hydrogen generation in-vessel, which can be estimated in a reasonably conservative manner, as outlined below.

The main core consists of 276 working fuel assemblies containing 126 fuel rods each. A fuel assembly is contained inside a hexagonal zirconium can. A 100% in-vessel zirconium oxidation (from fuel cladding and hexagonal cans) produces 800 kg H₂. The core contains 37 control assemblies located in a regular triangular grid. Below the main core there is a 0.45 m thick support plate of stainless steel. A special feature of the Loviisa core are the **fuel followers** attached to the control assemblies. The fuel follower is a fuel assembly of the same type as those of the main core, but somewhat shorter. When the control rods are inserted in the core, the fuel followers are vaporising water below the thick core support plate. Some 11% of the total decay heat is produced by the fuel followers. Even if the control rods melted at an early stage, the fuel followers would stay in place below the active core. They move inside guide tubes, and thus cannot drop down into the reactor pressure vessel lower plenum. This geometry implies that molten core material will not have easy access to the water pool **below** the core support plate. Therefore hydrogen generation in the long run will be limited by the steam generation below the active core. Table V shows how much hydrogen can be generated at different points of time, if all steam generated by the fuel followers is consumed in the zirconium-steam reaction.

Table V Steam and hydrogen generation rates as a function of time, $P_0 = 1500 \text{ MW}_{th}$

Time (s)	11% $\times P_{decay}$ (kW)	H ₂ O generation rate (kg/s)	H ₂ generation rate (kg/s)
18 000	1 440	0.72	0.08
36 000	960	0.48	0.05
86 400	720	0.36	0.04

As a **base case** for demonstrating the operation of the hydrogen management scheme, a 0.15 kg/s hydrogen release rate was selected. This is already 100% more than what would be expected from fuel follower steam generation. It takes about 5300 s to produce 800 kg hydrogen this way. As an **extreme case** we selected 0.3 kg/s hydrogen generation during 2700 s.

6 Recombination rates

Figure 11 shows the hydrogen molar fractions and volumetric loop flow rates in the containment for a **hydrogen release rate of 0.15 kg/s**, as predicted by "Virta". Half of the ice is assumed to remain when starting hydrogen injection. The ice configuration is nearly symmetrical with 60% ice left on the cold side and 40% on the hot side. The lower inlet doors are forced open to 50°, half of the intermediate deck and top deck doors are forced open. There is 60 m² inlet area of passive catalytic recombiners [10] in the lower compartment and 40 m² in the upper compartment. The steam release rate corresponds to a typical core uncover sequence decreasing from about 9 kg/s to 1 kg/s before the hydrogen release. After the hydrogen release we assume that the entire decay power is vaporizing water, thus the steam release rate is 5 kg/s.

The figure shows that hydrogen concentrations are controlled very well by passive catalytic recombiners in this case. The maximum hydrogen molar fraction (after releasing 800 kg hydrogen into the containment) is below 6% in the lower compartment and below 4.5% in the upper compartment. Thus, the criteria defined in chapter 4 are fulfilled with a wide margin.

The volumetric loop flow is functioning throughout the sequence. The maximum overpressure after the hydrogen release period is 0.24 bar, which is well below the containment design pressure. The figure also shows the injected volumetric rates of hydrogen and steam.

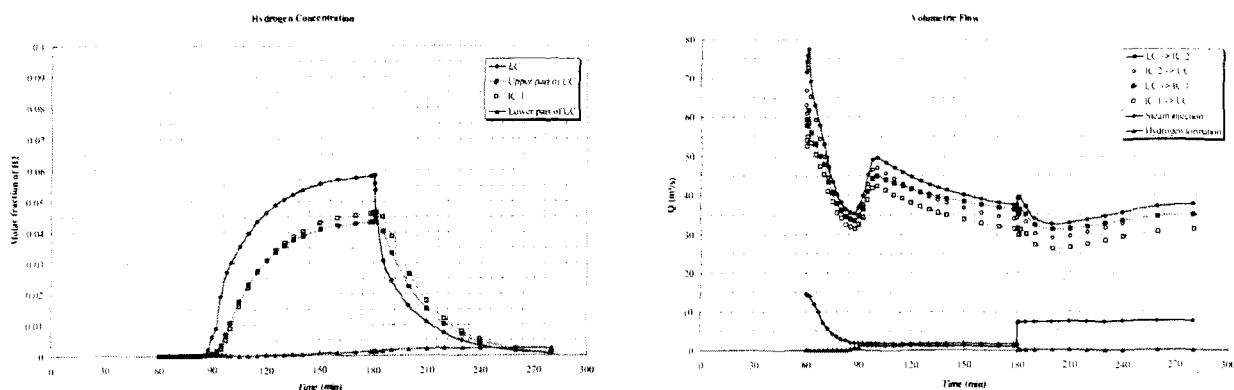


Figure 11 Base case hydrogen molar fractions and volumetric loop flow rates in the Loviisa containment

Figure 12 shows how the hydrogen management scheme works in the marginal case, with a **0.3 kg/s hydrogen release rate** during 2700 s, under the same scenario assumptions and recombiner capacity as the base case. The dome region molar fractions reach a maximum value of 7.5% and the lower compartment maximum molar fraction approaches 10%, while the steam

molar fraction is significant in the lower compartment, about 43%. The local criterion is exceeded at 126 minutes (when 75% of the zirconium has been oxidized), and the global criterion is approached at the end of the release. This parametric case was chosen to illustrate what would be required to produce significant hydrogen threat to the containment and to thus reveal the safety margins in the realistic base case. A thorough evaluation of safety margins, accounting for uncertainties in the ROAAM frame [11] is now being carried out for a complete documentation of the case.

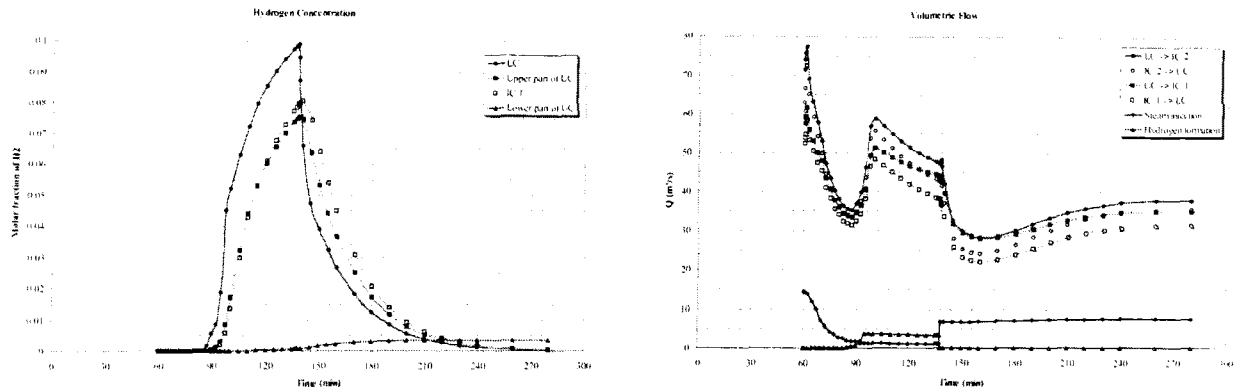


Figure 12 Marginal case hydrogen molar fractions and volumetric loop flow rates in the Loviisa containment

7 Conclusions

A new hydrogen management strategy is being developed for the Loviisa NPP as a part of a comprehensive SAM strategy.

The scheme is based on providing sufficient mixing of the containment atmosphere, effective energy removal from the containment, and controlled removal of hydrogen through passive catalytic recombination.

Development of the hydrogen management strategy was based on four logical steps. First, we demonstrated that a **global convective loop flow** will take place in the containment during hydrogen release, if the ice condenser doors are forced open as a SAM measure. The loop flow is driven by the difference in hydrostatic pressure in the ice condensers. We showed how volumetric flow rates could be quantified, and identified the most significant parameters affecting the loop flow rate. We could draw the conclusion that the UC dome region (above the IC outlet level) participated efficiently in this loop flow mixing process.

Second, we identified local and global hydrogen concentration limits, and concluded that the local criterion (9%) is limiting.

Third, we described the hydrogen source at Loviisa in a severe accident. As a base case for demonstrating the operation of the hydrogen management scheme, a 0.15 kg/s hydrogen release rate for 5300 s was selected. As a parametric case to test the failure boundaries, we selected 0.3 kg/s hydrogen generation during 2700 s.

Finally, the results from the first three steps were put together with the characteristics of a particular passive catalytic recombiner to dimension the system. We concluded that a total of 100 m² of inlet recombiner area would be quite adequate. Of it, 60% would be allocated to the lower compartment and 40% to the upper compartment. Finetuning and complete documentation of the case are underway.

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1.6 MELCOR 1.8.3 APPLICATION TO NUPEC M-7-1 TEST (ISP-35) AND TWO HYDROGEN SEVERE ACCIDENT SCENARIOS IN A TYPICAL PWR PLANT¹

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1.- INTRODUCTION.

Combustion of the hydrogen released to the containment during a severe accident is one of the issues to establish the real threats to the third barrier integrity in nuclear power facilities. Computational efforts on management procedures, such as the containment spray operation, are being addressed at the CTN-UPM to cope with the problem. On top of this, studies about in-containment hydrogen distribution and combustion are currently carried out with the codes MELCOR 1.8.3 and ESTER 1.0 - RALOC 2.2.

In this study, MELCOR 1.8.3 has been validated against the NUPEC M-7-1 Test (ISP-35), which already showed in 1993 that a good agreement was reached out when the previous MELCOR 1.8.2 calculations were performed regarding to the He distribution throughout the facility. Nevertheless, some discrepancies were detected when analysing wall and atmosphere temperatures. Generally, well-mixed atmosphere scenarios, in which the role played by the containment water spraying is of the major importance, appear when such a mechanism promotes the onset of convection-driven flow patterns that rapidly homogenize the gas properties. The purpose of the new MELCOR 1.8.3 assessment is to take advantage of the newest implemented models to obtain a more realistic thermalhydraulics simulation (in particular the water film-tracking model in the HS Package). A variation case was also performed to highlight the influence of water spray operation.

In a second part of the study, insights coming from the previous work were used to apply MELCOR 1.8.3 models to a SBO severe accident scenario management in a commercial 2700 MWt 3-loop W-PWR containment. A pseudo 3-D nodalisation is used to obtain a more realistic gas flow pattern simulation. Furthermore, critical parameters such as containment spray starting time in the sequence, effects of geometry and gas burn MELCOR 1.8.3 model user input parameters were studied to illustrate their crucial importance, since they strongly influence the way the combustible gas is released, distributed or burnt in every region of the containment.

Establishment of the kind of accident sequences that can be well simulated (and, on the other hand, those for which MELCOR 1.8.3 results are not reliable at all) by lumped-parameter codes is the background of this work, as is the chance of their improvement to make them able to simulate a wider set of situations, even involving

¹ This work has been performed under the sponsorship of the Spanish **Nuclear Safety Council** (CSN) to take part in the **MCAP** (MELCOR Cooperative Assessment Program)

large-scale gas unhomogeneities or thermal stratification. Identifying and solving these code shortcomings are the main concern for lumped-parameter code users.

2.- MELCOR 1.8.3 APPLICATION TO ISP-35 NUPEC M-7-1 TEST

A MELCOR nodalisation scheme of the NUPEC facility has been developed for gas distribution and mixing. This facility is a linear $\frac{1}{4}$ scale model of a typical 4-loop PWR containment, with a total inner volume of 1300 m³. During the M-7-1 test a steam-He mixture was injected inside the previously steam-preheated containment (He in an amount equivalent to the corresponding H₂ released through the 100% oxidation of the in-core fuel Zry) during a 30 minutes time interval; at the same time the containment sprays were functioning.

This nodalisation consists of 33 control volumes (CVs), 77 flow paths (FLs) and 124 heat structures (HSs) (Fig 1). Containment spray nozzles have been introduced through the SPR package making use of 8 water sources and 20 atmosphere spray junctions. In the HS package it has been taken advantage from the water film-tracking model (one of the novelties of the MELCOR 1.8.3 version), to accurately simulate the heat transfer between the walls and the containment atmosphere through the water film.

Inclusion of the film-tracking model calculations has the aim of simulating the effect that the sprayed water impingement on the containment walls has on the heat transfer. By means of geometrical considerations it was established that an 8.5% of the supplied water was directly sprinkled on the containment walls whilst a 1.6% fell on the inner compartment walls. This made the actual mass flow rate supplied to the containment atmosphere to be decreased in an 8.5%.

Within the framework depicted above the following sequences were calculated:

- The steam preheatup phase.
- The M-7-1 distribution and mixing test (main MELCOR assessment).
- Variation of the M-7-1 test inhibiting the water spraying (computational experiment).

2.1 Main Results

A good agreement was widely found between the experiment data and the numerical results for the M-7-1 test. The main conclusions are the following:

- Atmosphere temperature and pressure falls were slightly overpredicted because of the steam condensation and cooling enhancement made by the code treatment. MELCOR assumes that the water is supposed as being homogeneously distributed in the nozzle vicinity, which is clearly apart from the actual pseudo-conical shape of the spraying pattern that also prevents the inner water drops to strongly interact with the surrounding atmosphere. Relative errors layed about the 3% in both parameters. (Figs 2 and 3).
- The He concentration predictions showed to be well adapted to the experimental measurements (the maximum relative error did not raise above the 11%). In the He-

steam injection steam generator compartment, in which the highest released gas concentrations are found, calculations only diverged from the experiment in a 6% (relative error), which means about a 1% in absolute volume concentration (Fig 4). In the containment dome, which represents the 70% of the total free volume of the facility, the differences hardly reach the value of 4% of relative error in the most diverging case.

- Wall temperature predictions were highly accurated, showing the improvement of the wall water film-tracking calculations. Deviations from the experimental (EXPER) data were below the 1.5% and close to 0% in most of the cases, as shown on the FILM-T curve on Fig. 5. In the preceding figure is also represented the results obtained without the film-tracking calculations (No.F-T curve) for comparison.
- Calculations of the preheatup phase of the facility (210 minutes previous to the test) were also executed. Although experimental data were not available it could be deduced from the ISP-35 Final Report [1] that relative errors in the pressure peak estimation layed below 16% and those of the temperatures inside the 4%, margin values always overpredicted by the code. This study was focused on the establishment of the facility conditions previous to the test and to assess the code ability to simulate such a situation.

In the latest part of this work the same M-7-1 calculation was repeated, although without the water spraying. This computational test allowed to get some insight on the fact that water spraying promotes an important mechanism to cool and depressurize the containment atmosphere, but with the cost of rising the He volume concentration. Main results from this part were the following:

- The calculated values for pressure were up to a 27% higher than in the M-7-1 test, as shown on Fig 2 (MELCOR 1.8.3 set of curves for the M-7-1 test, No-SPR for the variation case). Furthermore, the containment atmosphere temperature at the end of the gas supply was about 7% above the M-7-1 values. Fig 3 reflects gas temperatures in the most critical control volume.
- He concentrations showed a meaningful increase when spraying, leading to differences of 30% for the maximum concentration values between both cases (Fig 4). In any case, the last calculation performed is affected by the lumped parameter code restrictions to simulate gas atmosphere conditions in which the absence of strong mixing and homogenizing mechanisms is their main feature.
- The steam mass present in containment is a 238% larger than that in the M-7-1 test, which means the effectiveness of water spraying for steam condensing. It is important to keep in mind the fact that the spraying flow rate of the M-7-1 test was overdimensioned in a factor of 4 respect to that of a typical 4-loop plant (water spraying flow rate was scaled by means of area, rather than by volume). Finally, the maximum oxygen concentration did not raise above the 16% in mole fraction, which is a value an 11% lower than in the M-7-1 test.

2.2 Conclusions

MELCOR 1.8.3 calculations performed for the NUPEC M-7-1 experiment have confirmed the code ability to make quite a good simulation of this test. Moreover, inclusion of the film-tracking model leads to a significant improvement of the results, demonstrating the more accurate solution of the heat transfer to the walls when water impinges on them.

As an outcome of this study, the MELCOR 1.8.3 seems to give some confidence when applied to the study of hydrogen behaviour and distribution under severe accident conditions with the water sprays active. Due to the lumped parameter feature of the code, those situations in which enhanced gas mixing mechanisms are expected will be predicted with fairly good degree of accuracy by MELCOR. Such mechanisms have been present during the M-7-1 test and include not only the intense water spray functioning, but also the location of the hot gas injection point in the lowest part of the facility and the gas flow freedom promoted by the open geometry. However applications to those accidental sequences too far away from the M-7-1 test conditions need some caution, as they could lead to poorly reliable solutions.

In the scope on a further and wider assessment of the MELCOR code within the gas mixing and distribution in containment, it would be relevant as a first step the code validation against other experiments similar to the NUPEC M-7-1, in particular those in which containment sprays were inhibited, shorter amounts of hydrogen (He) were released (about 30% of Zry oxidation) or with higher points of gas release. These factors would not enhance the atmosphere mixing and then would lead to large-scale unhomogeneous conditions or stratification inside the containment. In such situations, it would be useful to test the influence of a fine-node highly-detailed nodalisation scheme (mainly in the largest regions of the facility) and the need to the introduction of new models.

3.- MELCOR 1.8.3 APPLICATION TO TWO SEVERE ACCIDENT SEQUENCES WITHIN A TYPICAL W-PWR PLANT

The second part of the works at the CTN-UPM consists of the application of the gained MELCOR 1.8.3 experiences to a real plant in-containment scenario. The nuclear power facility selected is a typical 2700 MWt 3-loop W-PWR Spanish plant. First, a detailed 3-D nodalisation scheme was designed, in particular for those parts in which higher hydrogen buildups are expected. This nodalisation includes 46 CVs, 86 FLs and 137 HSs (Fig 6). Containment spray system has been collapsed to 6 spray sources. Furthermore, making use of the film-tracking model features, they have also been added as a water source those structures in the dome ceiling which condensate the steam and make it drop down as liquid water, as was taken into account the amount of water that was directly sprayed on the containment walls by geometrical considerations.

The hydrogen producing accidental scenario chosen is that of a station blackout (SBO) coincident with the seal failure of the three primary system coolant pumps (RCPSF). The source term released to the containment has been obtained by a previous MELCOR calculation at the CTN-UPM [2]. Main results of the study gave out a total amount of 270 kg of H₂ to be released through the SB-LOCA from t=6600 s to t=8700 s

in the sequence due to Zry-steam reaction in the core vessel. This yielded to a 36% of the initial in-fuel Zry inventory oxidation.

Within this initial conditions, several calculations have been carried out at the CTN-UPM. Special effort has been put on the establishment of the influence of several factors on hydrogen distribution and combustion:

- The instant at which the water spray system starts to operate during the sequence as an accident mitigation strategy.
- Effects of geometry to help gas mixing and spreading.
- The MELCOR burn model parameters to establish gas burn speed (depending on the geometry and kind of burn propagation) and propagation conditions to the other regions. This was made with the help of previous experience gained with the ESTER 1.0 - RALOC 2.2 code available at the CTN-UPM and additional bibliography. Used flame speed values were 30 m/s for lower containment and code default values for the open upper free volumes.

The preliminar "in-vessel" phase calculation of the SBO-RCPSF sequence started with the containment atmosphere under steam saturated conditions fixed by the maximum pressure (0.128 MPa) and minimum temperature (25 °C) given by the Technical Specifications. Furthermore, the above calculations led to the prediction of the vessel failure at $t=10000$ s (being $t=0$ the beginning of calculation and $t=500$ s the initial event). Two variation cases were then performed after this previous phase:

- In the first of them, degradation of the core progresses to vessel failure and cavity ablation by the debris, producing an additional amount of combustible gases (H_2 and CO) that is released to the containment up to complete (100%) oxidation of the initially existing Zry. Now it is when the external AC power supply is assumed to be recovered, and water sprays could be available to condensate steam and depressurize the atmosphere. This will be referred as SBO-RCPSF-MCCI sequence in the following.
- In the second one, the external AC power is recovered, which allows the plant operator to reflood with water the vessel just before its failure. Depending on the degradation status of the core, the remaining metallic Zry oxidation will be enhanced, producing additional H_2 amounts in-vessel (nevertheless, oxidation of more than 75% of the initial Zry looks quite unreliable under such scenario). At the same time, containment sprays will also be available. Information coming from previous studies at the INEL (USA) made with SCDAP/RELAP/MOD3 [3] has been used to make an estimation of the additional H_2 and steam sources from the primary circuit during the reflood phase. This will be named as a SBO-RCPSF-Reflood sequence.

3.1 SBO-RCPSF-MCCI Sequence. Main Results

3.1.1 Base Case. Case 1a

This scenario consists of the accident progression beyond the reactor vessel failure due to the unrecoverability of the lost inventory in the primary circuit by any coolant injection system. The containment spray system is also unavailable under SBO

conditions. Once the vessel failed, the external AC power is supposed to be recovered and the sprays may be on. The instant in which the water spraying is started is highly important for the released gas distribution and the likelihood of burns within the containment.

A first calculation was performed assuming the sprays inhibited during the whole hydrogen release (which takes place until the metallic phase layer in the cavity is extinguished. This occurs at $t=16800$ s in the calculation, but it is not important later than $t=15000$ s). When the MCCI phase of the sequence starts ($t=10000$ s) hydrogen and steam are released to the cavity, and the initially sealed connection to the lower containment is assumed to be opened. Local minor burns appear in the cavity, pushing the very hot gases away until the cavity atmosphere is oxygen-starved.

Gas is distributed to the regions above until a new burn starts at 14280 s in the lowest containment (CV-73. Fig 7 shows the H_2 fraction in the volume) which propagates to the whole building, producing a pressure peak of 0.55 MPa (Fig 9). About 450 kg of the released H_2 and 350 kg of CO burn in this sudden burst. No detonation model is available for the MELCOR BUR package, but the burn could be so violent to produce a higher pressure increase. Atmosphere gas temperature rises up to 1100 K (Fig 8). Gas cools slowly through the containment walls until water spraying starts at $t=22700$ s by user's choice. The cooling and depressurizing effect is clearly shown on the previous figures, but not too much in the lowest containment (CV-73) since very hot gas (mainly steam) is still coming from the cavity and is a bad vented region. One more issue is the fact that water spraying promotes gas mixing by introducing a larger heat sump at the top of the dome, trying to set a stable gas flow pattern with characteristic gas speeds about 0.5-0.8 m/s. No gas drawing or induced turbulences effects are simulated with MELCOR. No more important events take place regarding to hydrogen in the containment.

3.1.2 Effects of MELCOR BUR Package Parameters. Effects of the Computational Hardware. Cases 1b

Results presented above show to be highly sensitive to the user input in the MELCOR 1.8.3 BUR Package [4], in particular regarding to the TFRAC parameter (which accounts for how long must a burn progress in a region to be propagated to the next one) which ranges from 0.0 (immediate propagation) to 1.0 (propagation after burn is complete). Preceding results were obtained with TFRAC=0.7 for the lowest containment and propagation to the regions above, and fixed to TFRAC=0.3 for the free region. Larger values of TFRAC (0.75 or higher) inhibited the burn propagation to the higher regions, and lower values allowed faster propagations which yielded to gradual gas burns as it is released. In both of the cases, lower values of the pressure peak were observed (around 0.2-0.3 MPa).

So sensitive may be the code to this parameter that making calculations with the same user input on different machines may lead to different results. The base case was run on a DEC-Alpha 2000 OSF machine. But when done on a DEC-Alpha 2000 Open VMS version, a previous burn propagation pattern was restricted to the lower part of the containment ($t=13100$ s) and later on a second one affected to the whole facility

($t=14720$ s). The pressure peak value was, however, of 0.52 MPa in this case (similar to the one explained).

3.1.3 Effects of Water Spray Starting Time. Case 1c

As deduced from the sequence description, in the base case the water spraying does not influence (for hydrogen mitigation issues) once such a great burn has taken place. That is why an alternative calculation was done introducing the water sprays at $t=13000$ s, before the great burn is to be expected. It showed also irrelevant to perform the calculation for times longer than 30000 s, since no important events are expected anymore.

A new alternative case was then run in such conditions on a DEC-Alpha Open VMS version machine. The results are quite different to the base case. A series of local burns takes place in the lower containment from $t=13020$ s to $t=16890$ s. The pressure increase is shown on Fig 9 case 1c. When water sprays are on, two effects take place at the same time: gas mixing promotion and steam condensing. This makes pressure to decrease instantaneously, but also hydrogen concentration raises up to the burn limits (Fig 7), producing the onset of successive local burns that now extend to the lower containment. In this case the pressure only reaches a moderated value of hardly 0.24 Mpa (Fig 9), since combustible gas is gradually eliminated by flashing burns at the bottom and gas heat is rapidly removed by the sprayed water. About 390 kg of H_2 (410 of CO) disappear by burning. Fig 8 shows the gas temperature evolution in the lower region.

3.1.4 Effects of the Containment Geometry. Cases 1d

It has been arisen essential for the results above the fact that the combustible gas may reach the highest regions of the containment to allow the burns to be propagated to the whole containment, since they always started in the lowest part. So it makes necessary for the gas to find a way to spread through the uppermost containment. Variations in the flow area from the lowermost to the immediately above region (CVs 73 to 63), may also restrict the burn propagation patterns to the lower containment if this flow area is small (about 90m^2 for each one of the three sectors the containment is divided into) or allow the propagation to other regions if large (150m^2). Both cases led to low pressure peaks predictions (0.2 - 0.3 MPa). It is remarkable that the as-built area is 120m^2 originated 0.55 MPa, as already explained in case 1a.

3.1.5 Conclusions

After external AC power recovery, it has been studied the effect of water spraying beyond reactor vessel failure and combustible gas production by concrete attack. Gas mixing promotion effects have been addressed, though they are not predicted very effective in the lower containment. If local combustible gas buildups appear in such regions, it seems preferable starting water spraying as soon as possible. When concrete ablation has progressed far enough, gas explosions may be expected in any case. That is when local burns may be convenient to eliminate gradually the threat of a global explosion in the low bad-vented regions. Furthermore, water sprayed in the

atmosphere helps to gas cooling under such a burn as an additional energy sink.

Another features of code and plant modelling have also been highlighted. In particular, the need of burn model improvement in the case of MELCOR would be desirable to more detailed analysis of gas combustion phenomena in severe accidents. Experimental tasks may be helpful in the establishment of improved correlations for burn under different propagation regimes.

Influence of scaling and geometry in nuclear plant containments may be other of the issues to be in mind for future designs or improvements of the existing. Anyway, keeping apart detonation risk and some code limitations in H_2 distribution and combustion the large-dry concept of the most PWR containments show their ability as partial passive mitigation for hydrogen threats; combustible gas concentrations hardly reach values above 10%-12% in volume. This is close to the lower limits for spontaneous ignition, as generally assumed.

Table 1 shows a summary of the preceding main results.

3.2 The SBO-RCPSF-Reflooding Sequence

From the initial primary phase of the SBO-RCPSF accident, in this case it is assumed that external AC power to the plant is recovered before the core degradation may progress up to vessel failure. The plant operator may then try to recover the loss of primary circuit water by starting the HPCI system to the reactor vessel, simultaneously to the water spraying within the containment.

As published in a previous NUREG [3] documentation, reflooding of a partially degraded core implies an additional hydrogen generation in-vessel. The most unfavourable conditions arise when water addition is done to a very high temperature, in the time previous to its final collapse. Such a core may be subjected to temperatures about 1800 K. This is the core status that will be assumed in the present section at $t=9000$ s (some 17 minutes before the predicted vessel failure) when reflooding is initiated by the operator as well as the containment spray system.

From the same study it is derived that core reflooding must be made at a too strong injection rate for the HPCI system to remove the heat stored in the fuel and structures. This fact will produce intense Zry oxidation reactions in the presence of large amounts of steam. That is why the primary system is supposed to be depressurized to allow the accumulators to discharge cold borated water into the reactor core. This will produce the immediate evaporation of most of the water which will enhance the Zry oxidation. The power released by oxidation may be higher than that coming from decay heat in the fuel.

Based on the previous and other studies, it may be estimated that the initial phase of such a heated reactor core reflooding would enhance the remaining Zry oxidation in a lapse of time of a few minutes. Assumption of oxidation increase up to 70% in about 15 minutes seems quite reasonable. From this moment on, oxidation reactions are extinguished and the reactor cools down slowly by water boiling and steam release to the containment.

Under conditions exposed above, previous calculations were carried out at CTN-UPM to evaluate the threats to the containment. Sharp hydrogen concentration buildups were addressed in the whole atmosphere by establishment of stable circulation loops in the open free region, but not beyond the ignition limits. This is helped by water condensing and gas cooling provided by water spraying.

That was why a new calculation was performed under the new hypothesis of oxidation increase up to 85%, with the same assumed limits for hydrogen ignition. This value of oxidation seemed to be the lower limit in the plant to produce severe hydrogen burn scenarios within the containment. This will be named the base case for the SBO-RCPSF-Reflooding sequence.

3.2.1 Base Case. Case 2a

As said previously, several minutes before the vessel failure the operator is able to depressurize the system to allow the core reflood and to start the containment sprays. Additional hydrogen (340 kg) and steam are released to the containment in the following 900 s through the three main coolant pump seals. Water spraying helps and promotes gas mixing that set homogeneous conditions in the containment, except for the lowest part, lying out of the reach of the gas recirculation loops.

On the other hand, sprinkled water condensates the steam present, increasing the volume fraction of hydrogen and the other no condensible gases (Fig 10). This is done through the whole containment almost simultaneously, thus ignition limits may be reached out and burns start near any of the release locations. This occurs inside the pressurizer chamber just when hydrogen release is ending ($t=9900$ s). Moreover, as hydrogen composition in the containment is also close to the limits (above 7.5% in any case), flame is propagated through all directions (upwards from the bottom), with a total consumption of 535 kg of H_2 in 18 s. Gas temperature and pressure increase, reaching values of above 900 K and 0.56 MPa, respectively, shown on Figs 11 and 12.

Later on, the gas cools down helped by the sprays during their injection mode, and finally through the containment walls. No more remarkable events are predicted.

3.2.2 Effects of Spray Starting Time. Case 2b

This calculation tries to show the influence of the spray system initiation delay, making it to start 1000 s later than the AC recovery ($t=10000$ s). Hydrogen release coming from the reinitiated Zry oxidation by the fast core reflooding has just ended, and only steam is supplied out in the lowest containment. Hydrogen is pushed to the upper regions, and homogenization is achieved about 7.5% when containment sprays are on. Strong steam condensation in the uppermost regions and in the open free containment is arisen, while the lowest remains poorly influenced by the mixing circulation loop phenomena occurring above. This makes hydrogen presence in the atmosphere to be increased on and on (Fig 10), until ignition limits (10%) are reached out and burn is initiated at $t=15940$ s in the uppermost dome. Flame is then propagated downwards to the whole containment, since hydrogen composition is above the downward propagation limit (>9%) by means of the previous homogenizing mechanisms. Now gas temperature

arises up to 1200 K (Fig 11), probably because of the more rapid hydrogen consumption when flame is originated in the upper free region. Such a deflagration stands for 2-3 s, and eliminates 558 kg of H_2 , and seems to be more violent than that of the base case. However, the pressure peak in this case shows the same value (0.56 MPa), since it departs from a lower value of pressure (0.15 MPa) against the base case (0.23 MPa) as water sprays have been able to condensate steam during a longer time interval (about 6000 s). See Fig 12.

3.2.3 Effects of Spraying Without Combustion. Range of Uncertainties. Case 2c

In this variation case, it is supposed core reflooding but in-vessel Zr oxidation does no progress further than 70%. On top of that, lower limit for hydrogen ignition is decreased to 9% since a shorter additional release of this gas (260 kg) is expected. Containment sprays are started at $t=13000$ s in the sequence to establish the concentration increase by steam condensation.

The only remarkable feature is that pressure rise reaches 0.3 MPa (Fig 12) during the 900 s of the non condensible release and then descends as the still released steam is condensed on the containment structures, mainly in the upper dome. Falling droplets may help to further steam condensation as an additional spray source able to relax pressure down to 0.26 MPa in the time the containment sprays are activated. A sharper steam removal is addressed. This makes hydrogen composition (steady around the value 5.7%) to arise to 8.4% in the upper region, still below the lowered ignition limit (Fig 10). As gas mixing forces stand, the containment atmosphere homogenizes slowly in its lower parts. Temperature is shown on Fig 11.

3.2.4 Effects of Flame Speed Parameters. Case 2d

This execution corresponds to that of the base case, but with water spraying initiated at $t=14000$ s and MELCOR BUR Package flame speed parameters set to default values via code correlations. It must be underlined that previous cases used fixed user choice values (user experience). This case is thus similar to 2a (large hydrogen release) and 2b (delayed spray start, but even later). The aim was to address the influence of burn progression in the volumes in the flame propagation and maximum gas pressure and temperature (similar to case 2b). No meaningful additional results were given by this calculation, however. See Figs 10, 11 and 12.

3.2.5 Effects of Combustion at Low Concentrations. Case 2e

The last example corresponds to case 2c (70% Zr oxidation) modified to allow hydrogen burns at more lowered limits for ignition (now set to the maximum concentration predicted, 8.4%). Sprays are started at $t=9000$ s as in the base case. That is why hybrid results between cases 2a and 2c would be expected. In particular, burn limit is set to a critical value, so that burns will be only allowed when a significant amount of steam has been condensed (to say, later than a good degree of homogenization has been achieved). Hydrogen combustion starts then at $t=15400$ s in the uppermost regions. This is a delayed in time burn similar to that of case 2b (although spray action is quite early). The reason is that local hydrogen buildups that originated burns in the lowest containment in the case 2a now are not present. However, since now the mixture is

poorer in combustible gas, atmosphere temperature and pressure reach 1100 K and 0.5 MPa. Burn is also quite fast (it lasts for 3 s) although now 460 kg of hydrogen are burnt. Figs 10, 11 and 12 show the main parameter evolutions.

3.2.6 Conclusions

Containment atmosphere behaviour under reflooding scenarios may be observed from a different point of view as additional hydrogen release may be limited under the maximum degree of in-vessel Zry oxidation expected. In particular, MELCOR does not predicts harmful effects if oxidation does not exceed 70% and homogenization is enhanced by mixing mechanisms. However, it has been proven that spray operation may have the effect of modifying non-combustible mixtures to combustible ones.

Good venting of the gas released in local rooms to the open containment is desirable and may be helped by water spraying. Nevertheless, if such homogenization is obtained and gas burns become a real threat in any place, the high mass of gas released would promote a global deflagration. Containment sprays seem to be able to make a good mixing in about 1000 s or less. Steam condensing by water spraying is expected to increase hydrogen concentrations in an additional 3%. One more time, design of large free volumes for PWR containment behaves as a passive mitigation countermeasure for hydrogen buildups when local accumulations are avoided. More effective venting of lowest compartments should be desirable to eliminate their effect as burn initiators when expected.

Again, Table 1 summarizes the main results for the calculations done.

4.- ACKNOWLEDGEMENTS

Special thanks have to be given to the Japanese NUPEC's kind collaboration by providing the M-7-1 Test (ISP-35) data and results to CTN-UPM. Mixing NUPEC test were conducted under the support of the Japanese Ministry of International Trade and Industry (MITI).

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Case	Main Features	P_{max} MPa	T_{max} (local) K	Burn Start Time (local) s	Burn Duration s	H ₂ (CO) Burnt kg	Spray Effects
SBO - RCPSF - MCCI Sequence Vessel Failure $t=10000$ s Concrete Ablation: H ₂ (CO) mass: 740 (>450) kg							
1a	$t_{spr} = 22700$ s	0.55	1100	14284	9	450 (350)	Gas Cooling and Depressurization
1b	$t_{spr} = 22700$ s (OpenVMS)	0.52	1000	(13057) 14724	(1) 13		Similar to case 1a
1b'	TFRAC=0.6/0.75	0.2-0.3					
1c	$t_{spr} = 13000$ s	0.24	380 (1300)	(13022-16890)	(1-2)	390 (410)	Local Burn Promotion; Global Burn Avoiding
1d	$t_{spr} = 22700$ s (Sensitivity to flow areas)	0.2-0.3					
SBO - RCPSF - Reflooding Sequence $t=9000$ s Core reflooding & Water spraying In-vessel Zry oxid = 85% (610 kg of H ₂ , No CO)							
2a	As explained	0.56	920	9898	18	535	Cooling, Burn start in the bottom
2b	$t_{spr} = 10000$ s	0.56	1200	15940	2-3	558	Violent burn by homogenisation
2c	70% oxid, 9% lim, $t_{spr} = 13000$ s	0.3	420	No burns	None	None	Cooling, Depressurizing, Mixing
2d	Film speed (def), $t_{spr} = 14000$ s	0.56	>1200	18608	4	566	Similar to case 2b
2e	70% oxidation, 8.4% lim, $t_{spr} = 9000$ s	0.5	1100	15401	3	460	Similar to case 2b

Table 1. Main Results for SBO - RCPSF Sequences

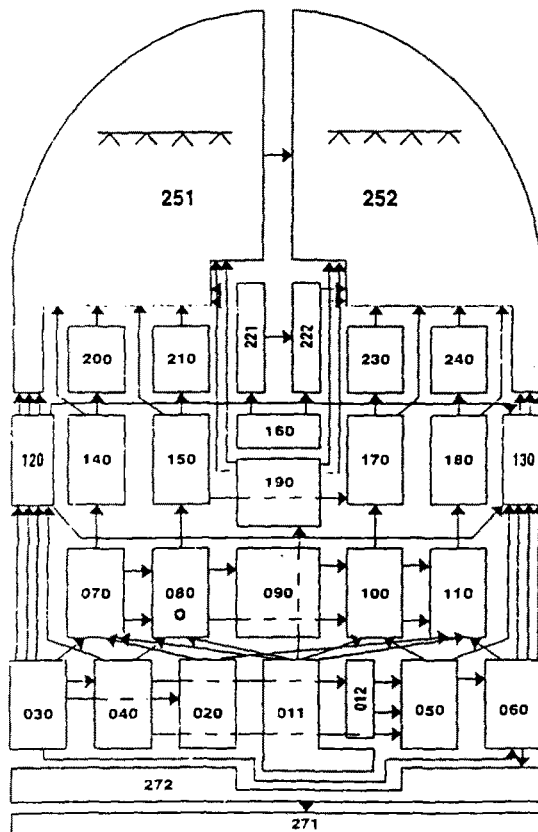


Fig. 1. NUPEC nodalization.

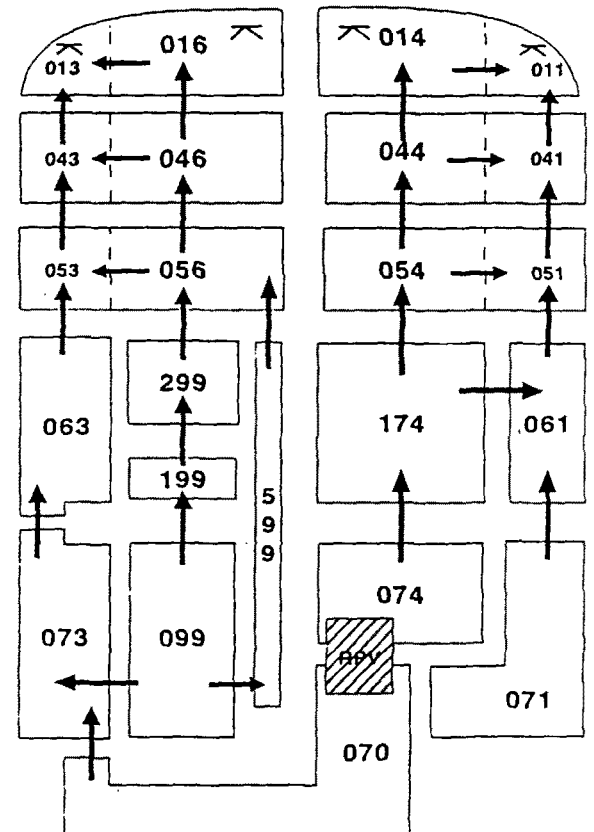
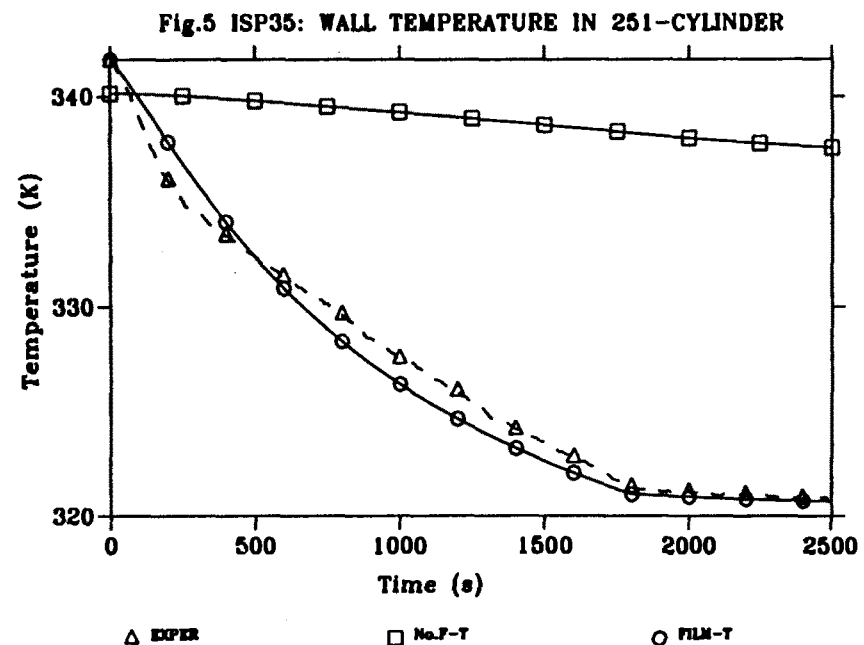
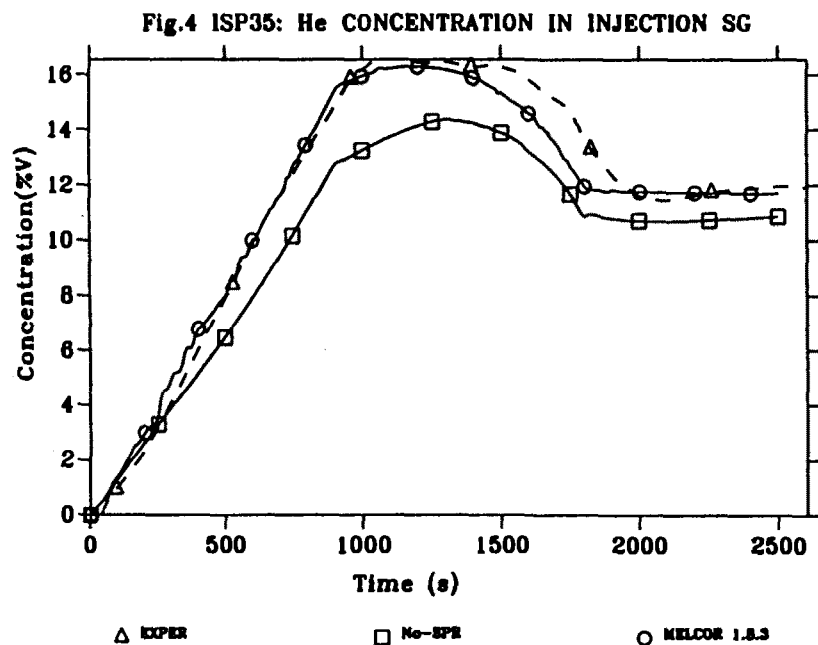
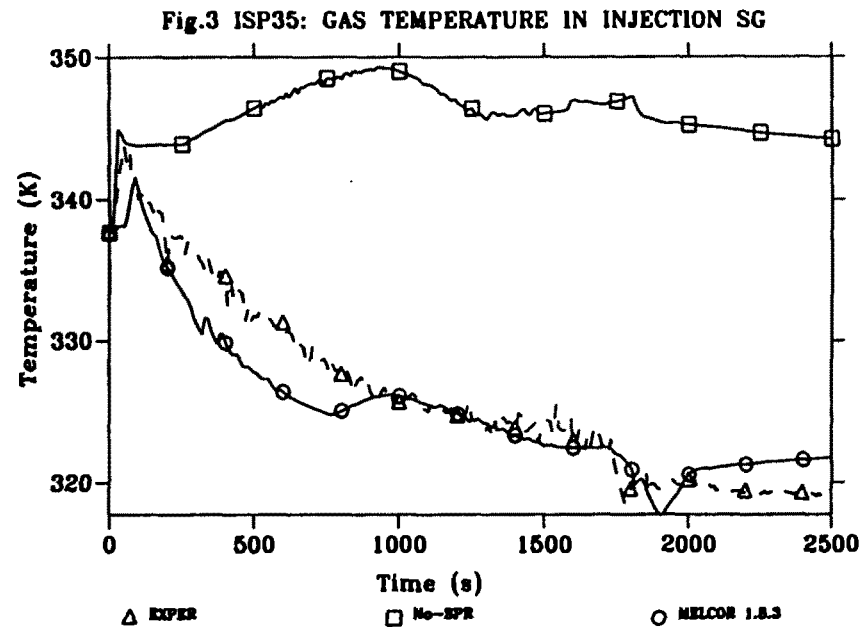
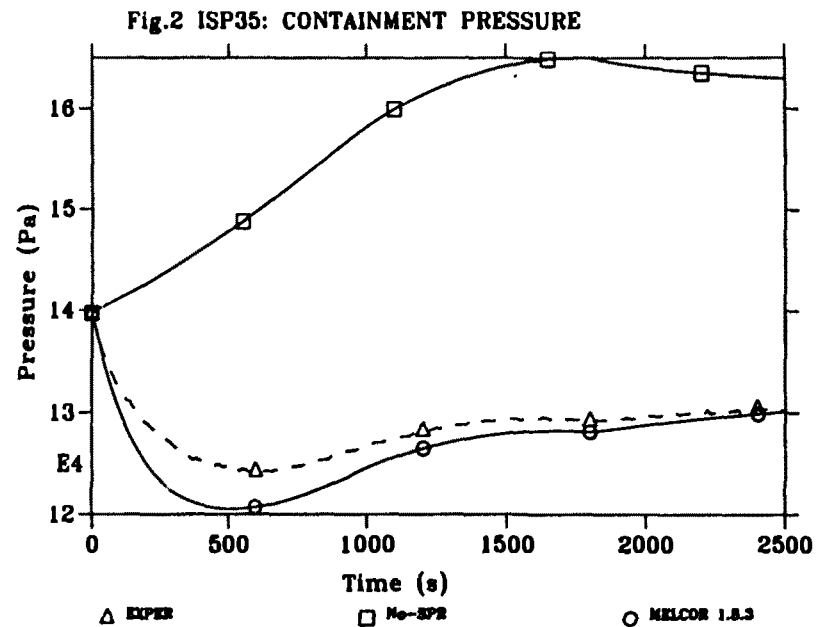
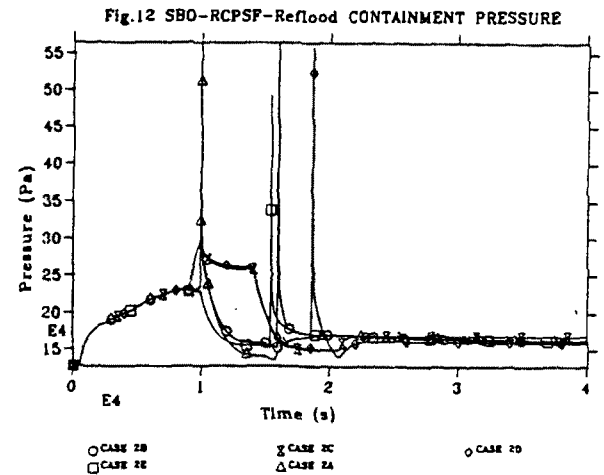
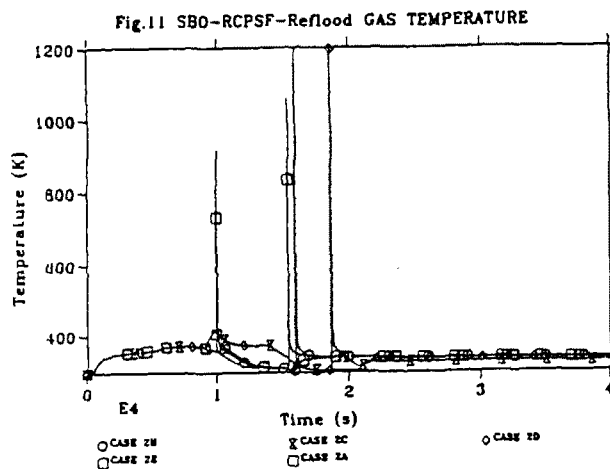
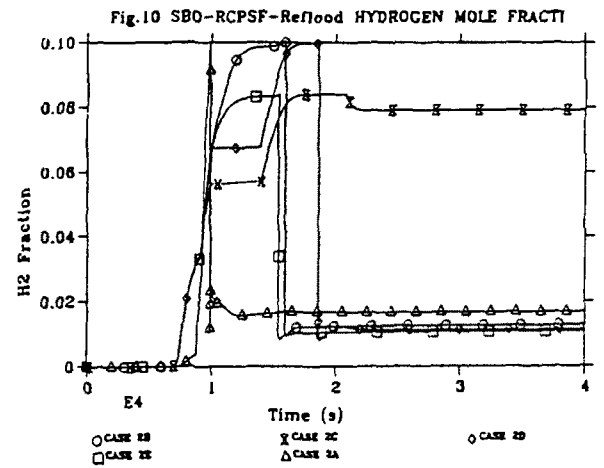
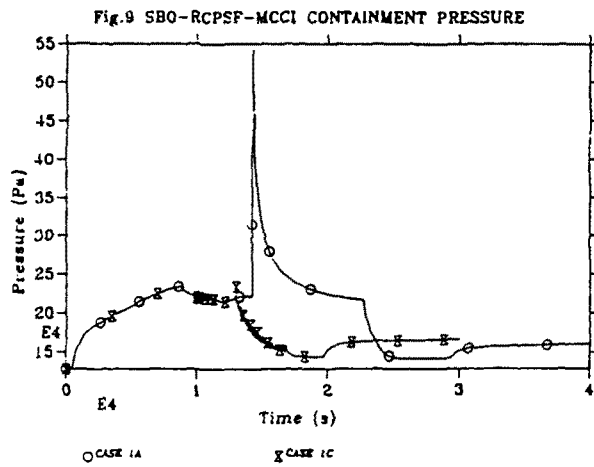
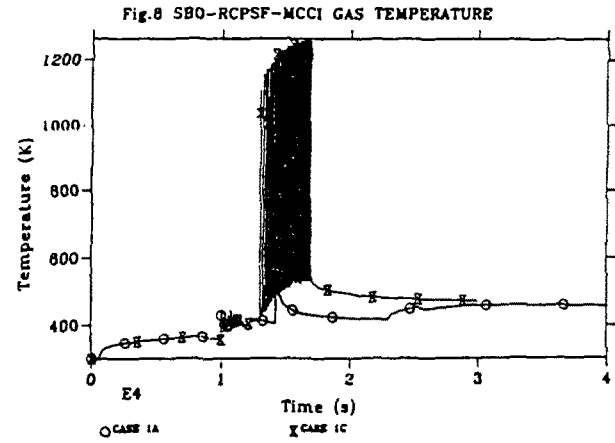
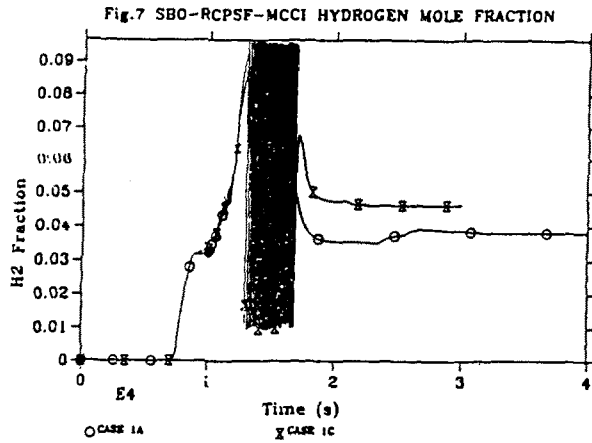


Fig. 6. W-PWR containment





SESSION II

**RELEVANT ASPECTS OF HYDROGEN PRODUCTION,
DISTRIBUTION AND MIXING**

Chairman: W. Frid (SKI, Sweden)

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2.1 CONCEPT FOR THE ANALYSIS OF HYDROGEN PROBLEMS IN NUCLEAR POWER PLANTS AFTER ACCIDENTS

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Abstract

After accidents in nuclear power plants, which lead to a overheating of the core up to a partial or complete core melting, hydrogen is produced due to the reaction of fuel cladding and other metallic structures of the core with the cooling water. This hydrogen enters the containment through a leak of the primary system or at reactor pressure vessel failure. The danger of fast deflagration or explosions appears which may affect the containment.

For the analysis of the containment phenomena two different types of computer codes are used, lumped parameter codes and 3D codes. This paper describes the advantages and the limitations of both methods. The codes used by Siemens KWU are presented with some examples for hydrogen analyses. A prospect of further development is given.

1. Introduction

Loss of coolant accidents in nuclear power plants lead to a release of hydrogen from the primary circuit. If the consequences of the event are limited, that means that the event is in the frame of the design and the cladding temperature remains in specified limits, the amount of hydrogen released into the containment and the release rates are low. Accidents in nuclear power plants beyond the design basis, which lead to a overheating of the core up to a partial or complete core melting, produce hydrogen due to the reaction of fuel cladding and other metallic structures of the core with the cooling water. In this case, the amount of produced and released hydrogen is much higher compared with the

design base accident. If this hydrogen enters the containment through the leak or at the reactor pressure vessel failure, the danger of fast deflagration or explosions appears which may affect the containment.

To be able to include corresponding counter-measures meaningfully, the following questions must be answered by the analysis:

- How does the hydrogen distribute in the building? What are the boundary conditions under which hydrogen accumulations are expected and how they can be avoided.
- What are the effects of the counter measures? Possible counter measures are those which eliminate the hydrogen, e.g. recombiners and igniters, and those which dilute the hydrogen or inert the containment atmosphere.
- What happens in case of an unintentional ignition?

The hydrogen production rates will be given by the „in - vessel“ analysis of core heating and the following behaviour of the corium up to the interaction with the concrete of the base floor of the containment. The questions above will be answered by the computer codes for the thermodynamic containment analysis. This paper deals with the requirements and the different approaches in containment analysis.

2. Requirements for the Analysis of Hydrogen Mitigation Techniques

To avoid a severe damage of the containment and the release of fission products to the environment fast deflagrations and explosions must be prevented. For this purpose a sufficient hydrogen mitigation technique for a wide range of probable accident scenarios had to be chosen.

The main question for the choice of an appropriate mitigation technique is the distribution of hydrogen in the containment. The analysis had to describe the gas distribution with a sufficient exactness during the entire accident. The used analytical tools have to describe the following phenomena with sufficient precision:

- Both slow and fast distribution of gas components, steam and water droplets,
- Thermodynamic effects like temperature stratification and condensation,
- Heat transfer to walls and structures,
- Convection produced by the effect of the recombiners as well as the consequences of the recombination to the hydrogen distribution and the thermodynamical conditions in the containment,
- The effect of igniters,
- The feeding of inerting gases with their side effects and their distribution in the containment,
- Slow and fast deflagrations.

With an efficient analytical tool the positions of igniters and recombiners can be optimized. A precise computational analysis with a validated code can partly replace expensive experiments.

3. Possible Tools for the Analysis

The computer codes used for the analysis are either „lumped parameter codes“, 3D-codes or a combination of both.

3.1. Lumped Parameter Codes

In lumped parameter codes the whole containment is simulated by a couple of zones, for each of which homogeneous conditions are assumed. A mass and an energy balance is prepared for each control volume. Since the individual balance zones are interconnected, they are linked to each other via a transport of mass and energy. The zones are connected by free openings, rupture discs, vents etc. for the exchange of the gases. Sump water in the nodes can interact with the atmosphere. Heat transfer to walls and structures as well as heat conduction through walls is taken into account.

The emphasis of lumped parameter codes is clearly on the description of the thermodynamics in the zones. Fluid-dynamic phenomena are simulated as a transfer of mass and energy between the zones. If an adequately large number of zones is selected, complicated convection phenomena can also be simulated.

Lumped parameter codes are used to analyse longterm accidents in acceptable computing time (less than the real time of the problem). The accuracy of the results is limited by nodalisation. Larger volumes can be divided in several nodes, if temperature stratifications or different gas distributions are anticipated.

3.2. 3D-Codes

3D-Codes use finite volumes to describe the whole containment. Since the number of meshes are several thousands, they are able to predict precisely the gas distributions even in such cases where a lumped parameter code fails. Those cases are the distribution nearby the sources, in gas jets or when combustions occur.

The use of 3D-codes is limited by the size of the meshing because of the computing time which is needed for the calculations.

3.3 The combination of lumped parameter code and 3D-code

The combination of both types of computer codes can combine the advantages of each of them. The lumped parameter code is used to analyse the total accident in a more global way, while the 3D-code is used to analyse special phenomena over limited periods.

The link between both computer codes can be either a off line or a direct on line. The calculated thermodynamic conditions and gas distributions from the lumped parameter code can be used as initial values for the analysis of a hydrogen combustion for example. The results of the 3D-analysis then can be used to fit the lumped parameter code. This is an example for a very weak coupling between the codes.

With a real coupling of both codes it is possible to switch over to a 3D-calculation in several zones when ignition limits are reached. Then, for example, the hydrogen combustion is calculated in a 3D-code simultaneously to the lumped parameter calculation of the rest of the containment. The results of the 3D-analysis (pressure in the zone, average temperature, gas flows out of the zones, gas masses in the zone) are given back to the lumped parameter code after each time step.

4. Codes used by Siemens KWU

4.1 The Lumped Parameter Code WAVCO

The lumped parameter code WAVCO has been developed by Siemens KWU to predict thermodynamic conditions and hydrogen distributions in containments after severe accidents. In Fig. 1 the effects considered in the code are shown. Especially for the hydrogen problems autocatalytic recombiners as well as thermal recombiners can be modeled.

WAVCO is used by Siemens KWU for the analyses of the thermodynamic consequences of severe loss-of-coolant accidents in nuclear power plants (PWR and BWR) as well as for computational predictions of the distribution of both the flammable gases H_2 and CO as well as the inert components N_2 , CO_2 and steam.

When designing the related models, considerations were not restricted to the fundamentally planned application in the severe accident sector, rather an attempt was made to take account of phenomena which primarily occur in the case of the controlled loss-of-coolant accident (LOCA) in order to develop the most

universal possible computer program in this way. This includes the capacity to model both extremely fast and extremely slow processes.

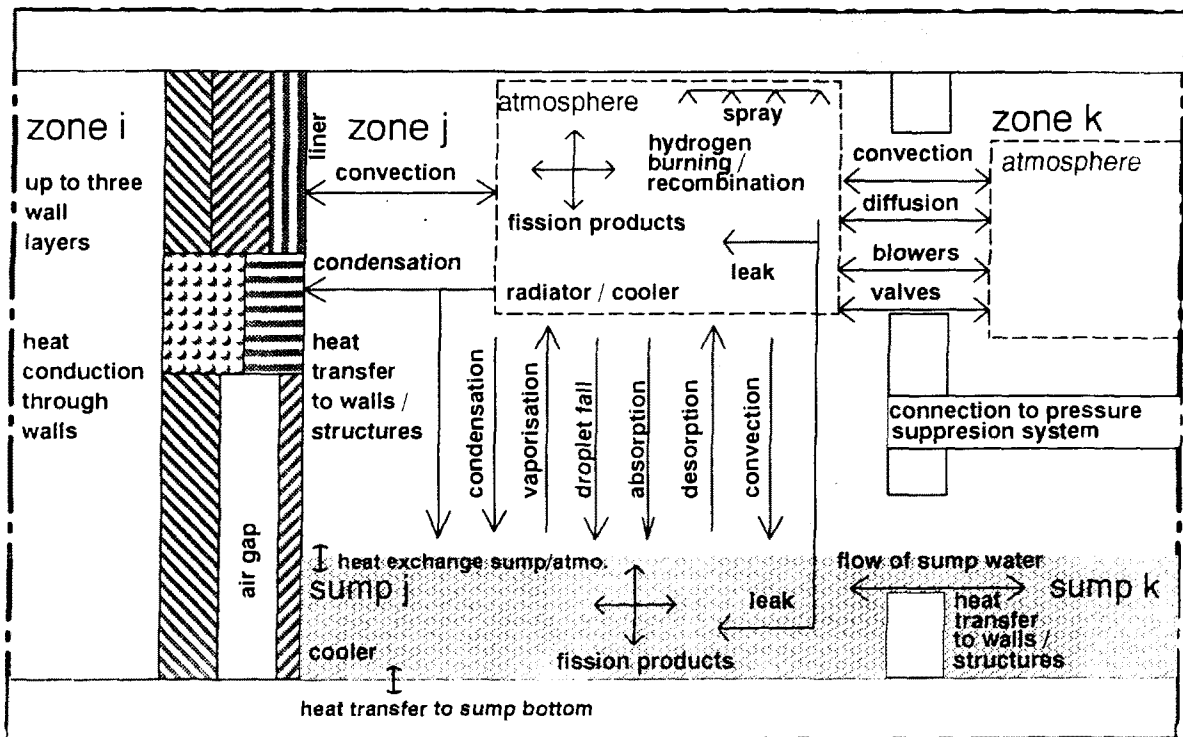


Figure 1. Effects Considered in WAVCO

As an example for the analysis of hydrogen mitigation measures a worst case scenario (from the point of hydrogen production) in a KWU 1300 MW PWR is described here. The accident is a medium break LOCA (50 cm² in the cold leg) with a total failure of the RHR-system. Only the eight accumulators feed their water into the primary circuit. Additional to the failure of all active RHR-systems the secondary cooldown and the primary pressure relief fails. At the most unfavourable time, when the core is heated up but not molten, a reflooding of the core is assumed. As a conservative assumption all zircaloy in the core react with the water and steam produced by the reflooding procedure.

Figure 2 shows the nodalisation scheme for the WAVCO - calculation.

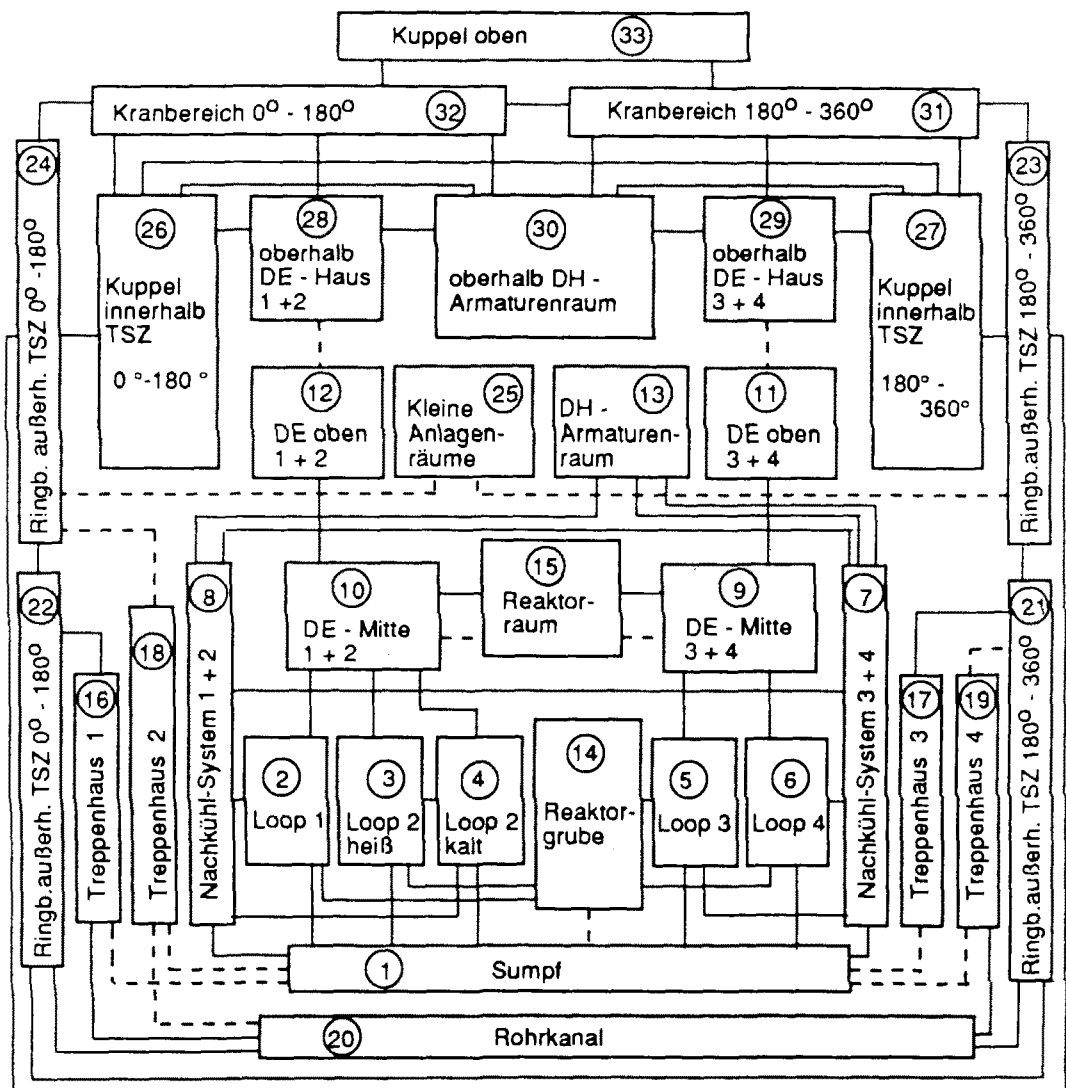


Figure 2. Nodalisation Scheme for the WAVCO - calculation for a PWR

The first mitigation measure analysed are autocatalytic recombiners. For the chosen accident scenario this measure alone can not avoid flammable gas composition in certain periods of time . Fig. 3 shows the gas composition for one upper steam generator compartment.

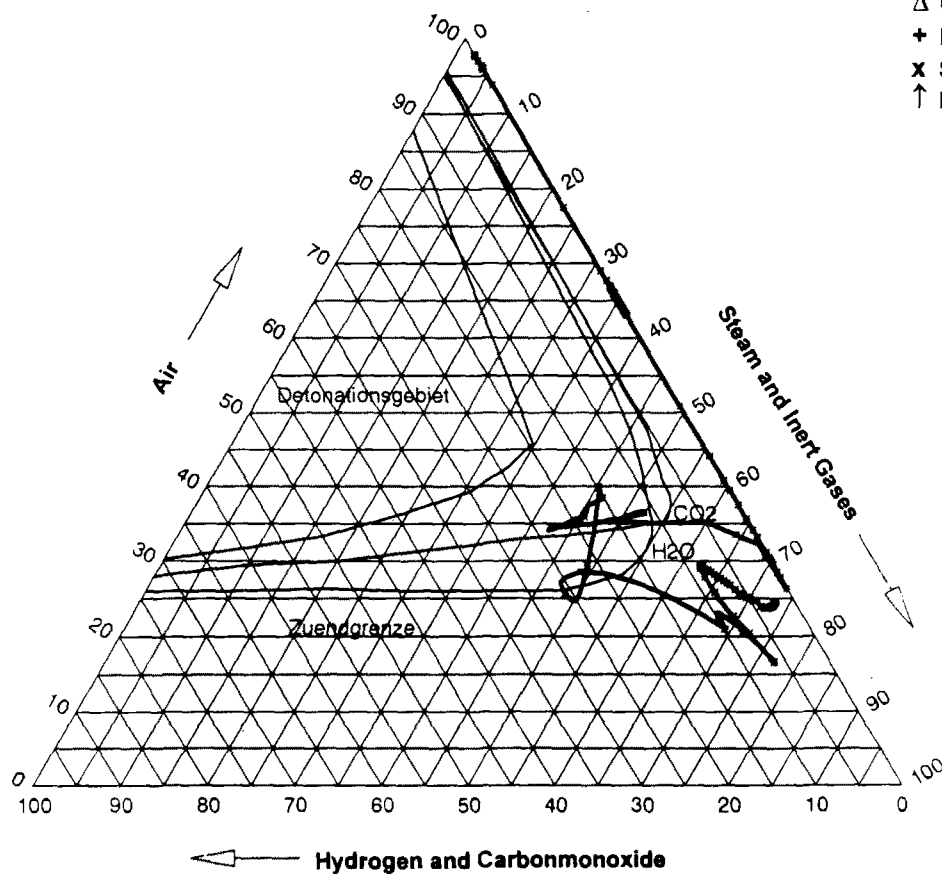
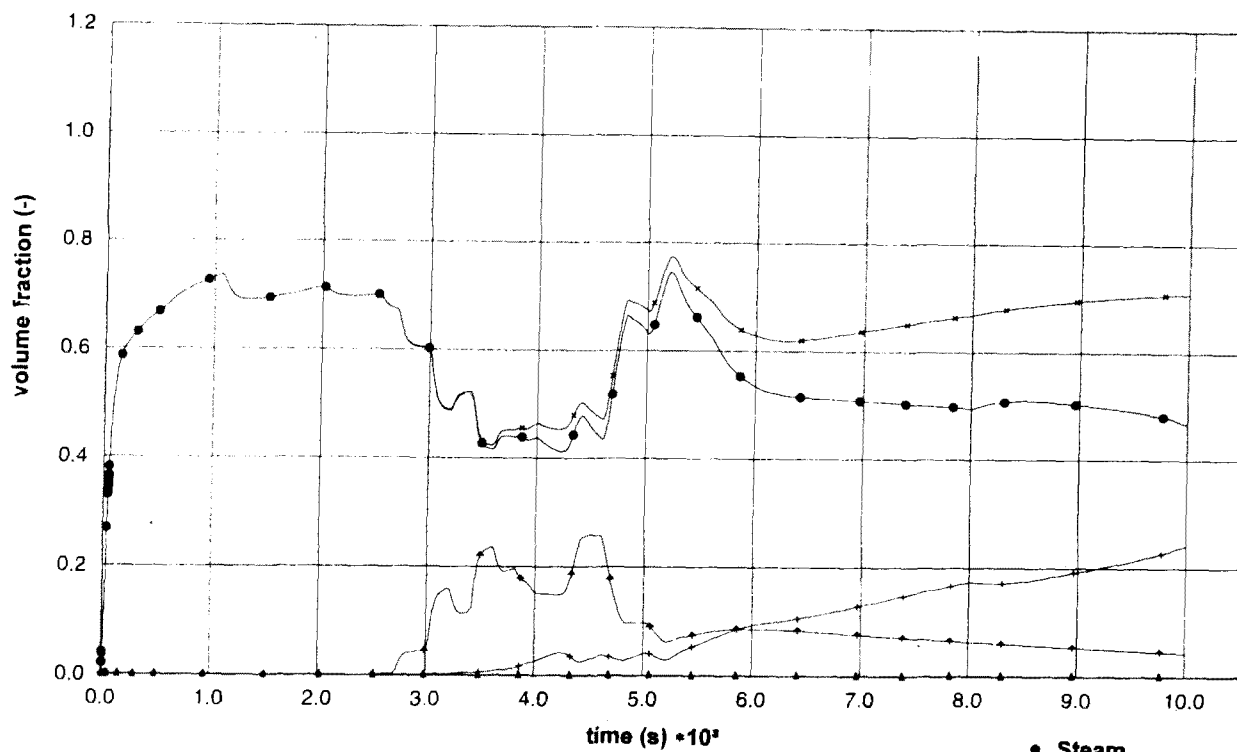


**Figure 3. Gas Composition in the Upper Steam Generator Compartment
42 Autocatalytic Recombiners**

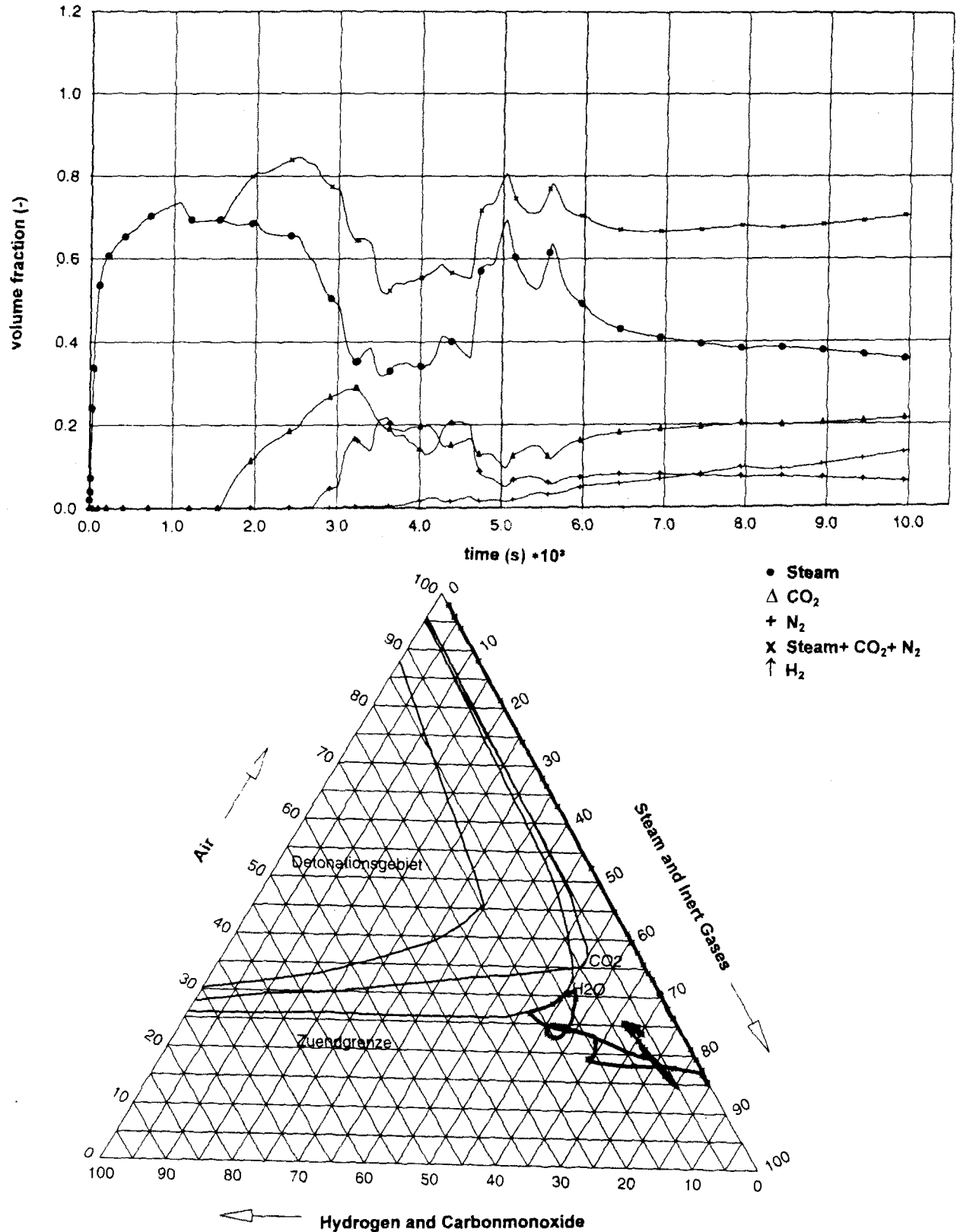


**Figure 4. Gas Composition in the Upper Steam Generator Compartment
20 Autocatalytic Recombiners and CO₂ - Dilution**

For the second calculation the number of recombiners was reduced and additionally the containment atmosphere was postdiluted by feeding with carbon dioxide. The effect of this measure on the gas composition for the upper steam generator compartment and the upper dome is shown in Fig. 4. The area of slow deflagration is only touched in this case.

4.2 The 3D-Code FLUTAN

FLUTAN is an improved FZK development of the ANL code COMMIX. In the frame of research and development contract with FZK the FLUTAN code has been transferred to Siemens KWU.

At Siemens KWU the code has been extended for hydrogen combustion by introducing an eddy dissipation model. The model has been validated by the Battelle Model Containment (BMC) experiments IX4 and IX6, [1] and [2]. These calculations will be shown here.

The BMC is a cylindrical concrete containment of 12 m diameter, 10 m height and about 640 m³ total volume. The experiments IX4 and IX6 took place in two compartments of 40 m³ each. The ignition was in compartment 1 which was connected to compartment 2. The opening area between both compartments was 0,28 m² and the rest of the BMC volume of about 560 m³ was connected to the top exit of the second compartment of 1,8 m² section area, which initially was closed by a foil.

The experiments IX4 and IX6 were chosen of a series of H₂ deflagration experiments in BMC because of the following reasons:

- the geometry of the experiments was the same,
- the two compartments were filled with 10 % H₂ initially in both experiments,
- only the position of the igniter in compartment 1 was different.

In IX4 the igniter was positioned near the wall opposite to the connection of the compartments separated by a foil initially. In IX6 it was near the connection which was open initially.

The experimental results of these experiments were totally different. Experiment IX4 showed a transition from slow deflagration in compartment 1 initially ignited to the development of a jet ignition in the compartment connected. In experiment IX6 only a weak flame acceleration was observed. In both cases the gas was driven out into the rest volume, when the foil at the exit of compartment 2 was torn.

First a three dimensional calculation was carried out which gave good agreement with the measured pressure transient. This calculation was very time consuming. So it was important to get reasonable computing times. This could be achieved by reducing the number of cells. Besides the use of coarser three dimensional meshes it is very interesting to step even to two dimensional meshes. In the experiment under investigation the radial dimension is relatively small compared to the circumferential dimension, so also a two dimensional calculation should give good results. Fig. 5 shows the comparison of calculated and measured pressure transient for the experiment IX4 for the two dimensional calculation. The pressure buildt up is in very good agreement between calculation and experiment.

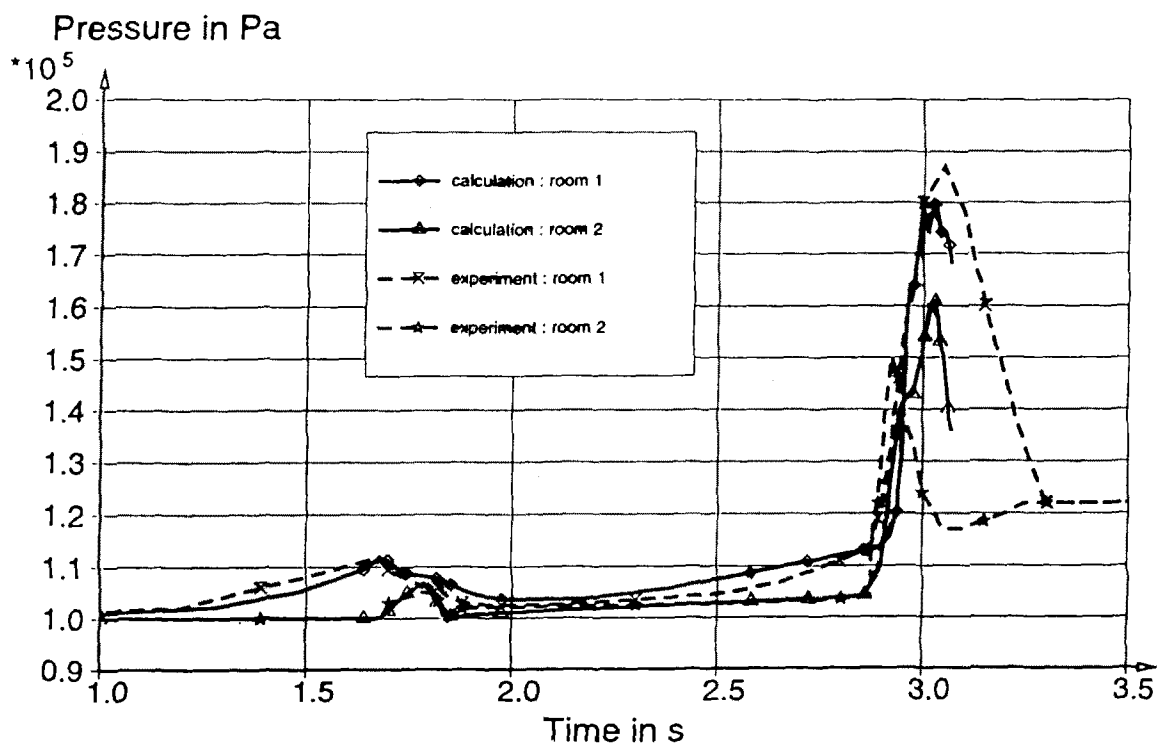


Figure 5. Hydrogen Deflagration. Calculated versus Experimental Data, IX4

In further steps the number of computational cells was diminished from about 14000 to 200 cells. The pressure course, the flow and flame velocities measured in IX4 as well as in IX6 were well reproduced with this model. This was an important step to reduce computing time. The calculations of a fine 2D mesh two compartment model which was used to define the empirical parameter of the combustion model, could be verified even for a variety of coarser meshes in a three compartment model and combinations. With the IX6 experiment the slow deflagration modelisation was confirmed.

5. Comparison of a Hydrogen Distribution Calculation with WAVCO and FLUTAN

A comparison of a hydrogen distribution problem calculation with the lumped parameter code WAVCO and the 3D code FLUTAN was performed to show the advantages of a 3D nodalisation and the limitations of a lumped parameter code in this field. Following an accident scenario with bleeding of the primary circuit (ND*-scenario) the phase of hydrogen release from the pressurizer relief tank was chosen for the comparison. Simple initial and boundary conditions are chosen to explain clearly the differences between the results. The calculations are started in a hot containment atmosphere which is homogenous over the whole containment.

For the WAVCO calculation the same nodalisation as described in chapter 4.1 is used. The FLUTAN calculation is made in a nodalisation of 3025 computational cells (for one half of the containment).

A constant rate of 7.8 kg/s steam and 0.15 kg/s hydrogen with a temperature of 405 °C is released into the containment over a period of 500 s (FLUTAN: one half of the values). After this period the release rates are shut down and a phase of homogenisation of the containment atmosphere by balancing processes is expected.

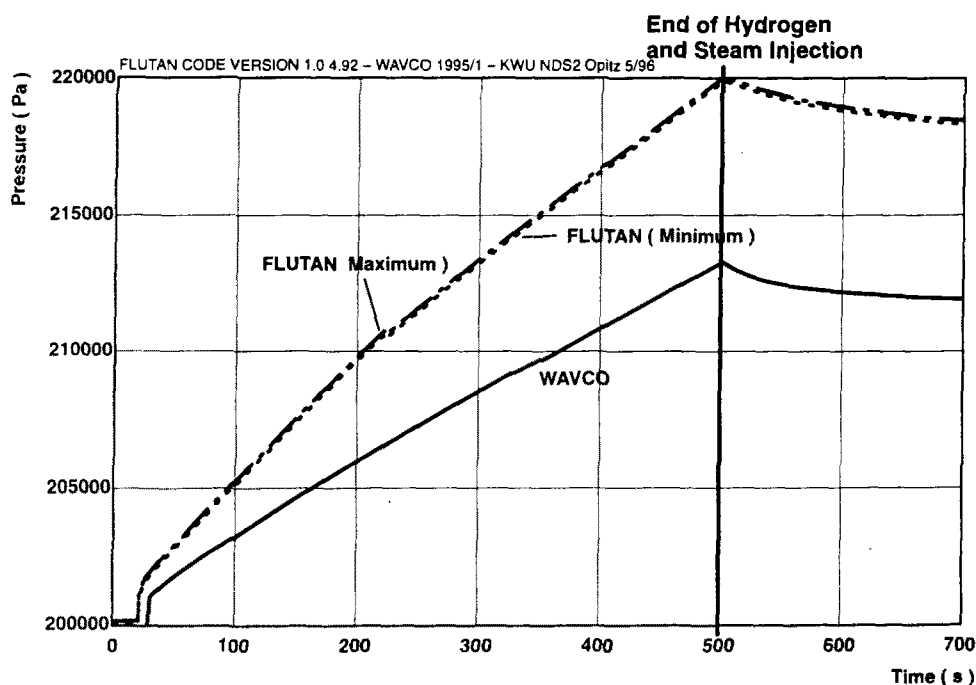


Figure 6. Pressure in the containment calculated with WAVCO and FLUTAN

Fig. 6 shows the pressure in the containment in the dome over the time calculated by WAVCO and FLUTAN. The differences between the results of ca 7500 Pa = 75 mbar can be explained.

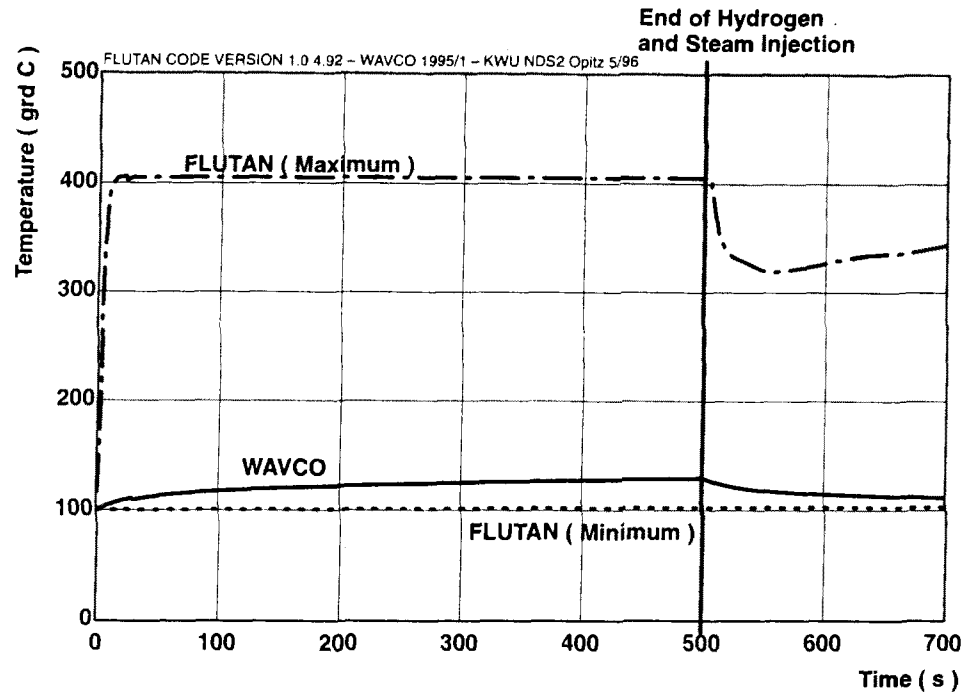


Figure 7. Temperatures in the Atmosphere of the Release Zone calculated with WAVCO and FLUTAN

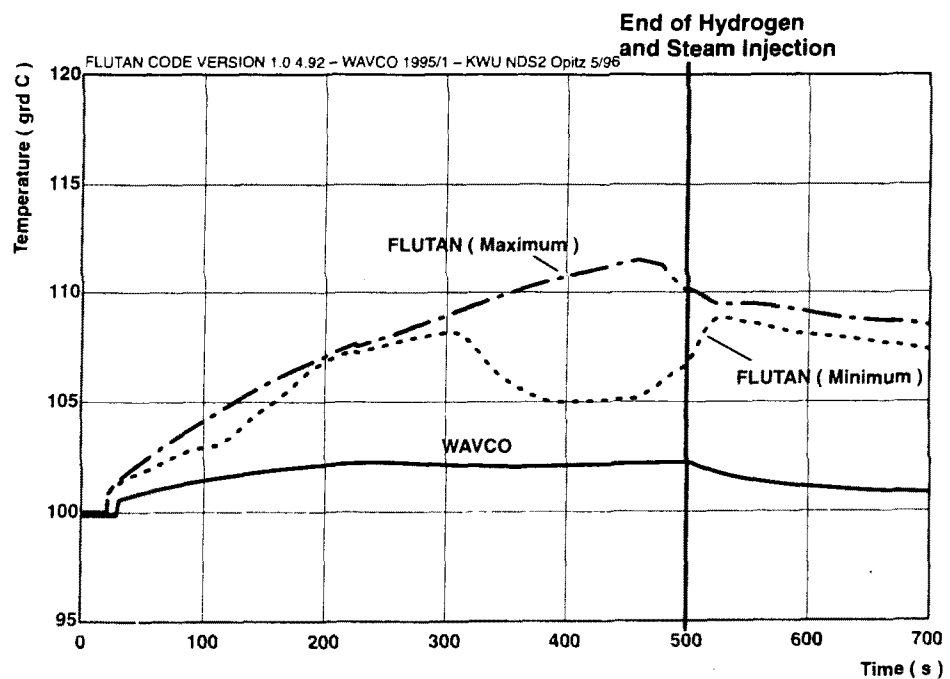


Figure 8. Temperatures in the Atmosphere of the Dome calculated with WAVCO and FLUTAN

As shown in Fig. 7 FLUTAN calculates temperature distributions in the zones. The temperature in the path of the released hot gases is significant higher than an average temperature in the zone calculated by a lumped parameter code. The temperature near walls is a lot lower than the average temperature and for this reason the heat transfer to walls calculated with the 3D-code must be lower than those calculated by the lumped parameter code with the same heat transfer coefficients. This effect is important near the source of hot gases only. In zones which are far away from the release point (see Fig. 8), the temperatures are nearly homogeneous over the whole zone and this effect did not occur. From this result it can be suggested that a fine nodalisation up to a 3D-nodalisation near sources of hot gases has advantages in calculation of proper pressure and temperatures.

In Fig. 9 and Fig. 10 the hydrogen distribution in the dome and in the release compartment is shown. As expected considerable differences between the volume fractions in several meshes of the FLUTAN calculation exist in the release compartment. In the dome, a zone far away from the source, the differences are minor and the volume fraction calculated with WAVCO gives a good approximation.

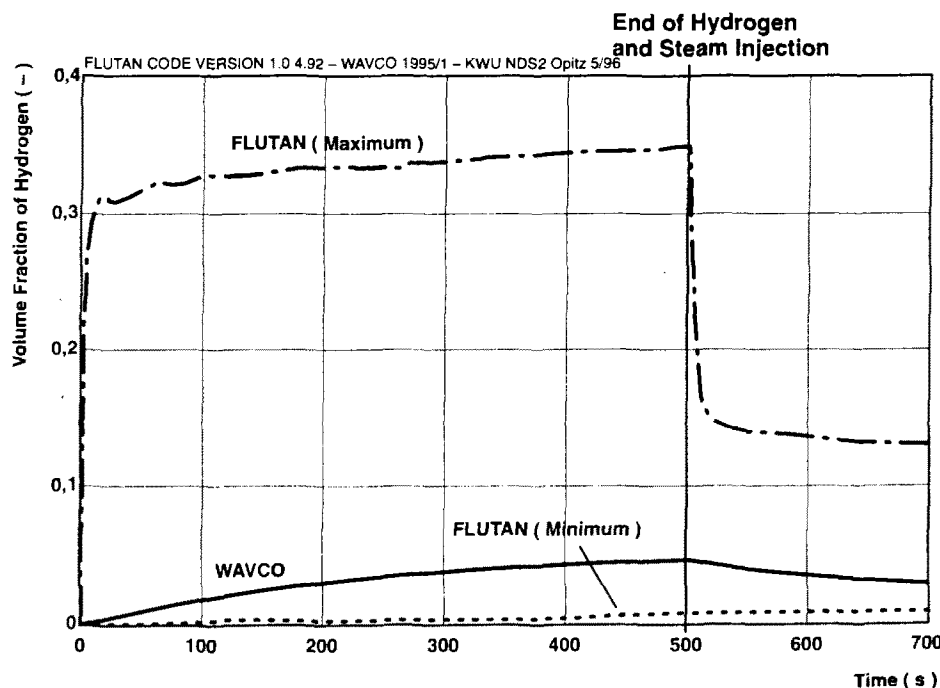


Figure 9. Volume fraction of hydrogen in the release compartment calculated with WAVCO and FLUTAN

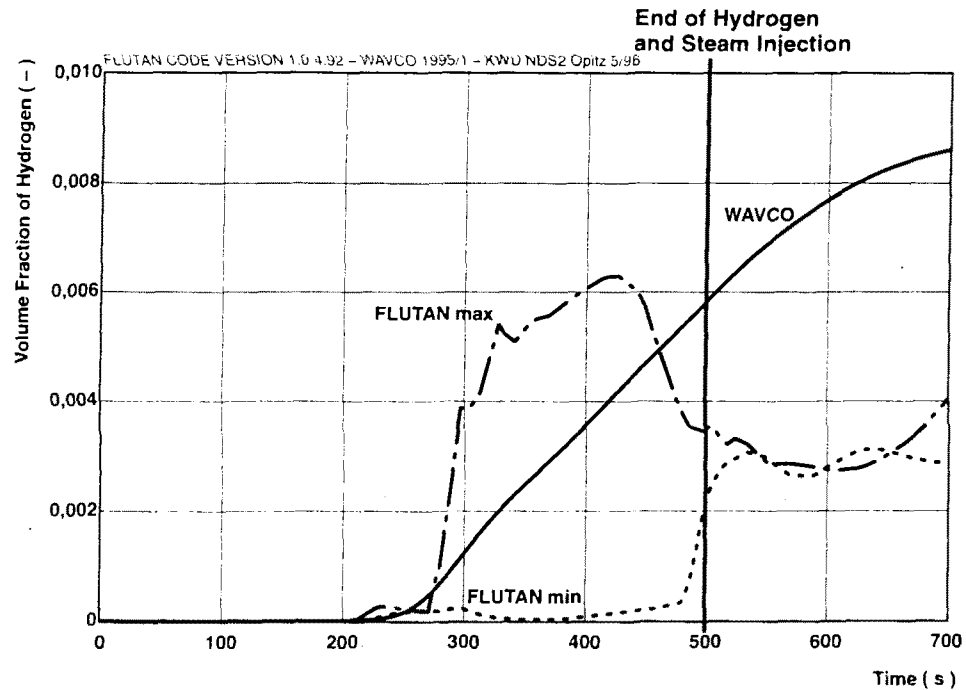


Figure 10. Volume fraction of hydrogen in the dome calculated with WAVCO and FLUTAN

6. Future Development

Up to now the lumped parameter code WAVCO and the 3D code FLUTAN are both used as stand alone codes. The coupling of these codes is realized at present.

The coupling will be done in such a way, that for every zone or for some zones of the lumped parameter code a nodalisation for the 3D-code exists. If certain limits in the zone are reached (i.e. the ignition limits or gas releases) the calculation switch over from the lumped parameter code to the 3D option in this zone. The results of the 3D calculation are averaged and given back to the lumped parameter code. When the transient phase (hydrogen combustion or transient gas release) has been terminated the calculation is continued with the lumped parameter code.

This approval will combine the advantages of the lumped parameter code (sufficient exactness with low computing time) with those from the 3D code. Local phenomena which can not be described in a lumped parameter code with sufficient accuracy are analysed with the 3D code. These phenomena occur only

over short time periods compared to the whole accident scenario. Only these short periods and only several zones will be analysed with the large effort of a 3D calculation. The overall accident is analysed with the lumped parameter code. Thus it is possible depending on the requirements to analyse the different accidents with the optimum approach concerning accuracy and computing time.

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2.2 ANALYSIS OF MITIGATING MEASURES DURING STEAM/HYDROGEN DISTRIBUTIONS IN NUCLEAR REACTOR CONTAINMENTS WITH THE 3D FIELD CODE GASFLOW

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Abstract: This paper reports on the recent model additions to the 3D field code GASFLOW and on validation and application analyses for steam/hydrogen transport with inclusion of mitigation measures. The results of the 3D field simulation of the HDR test E11.2 are summarized. Results from scoping analyses that simulate different modes of CO₂ inertization for conditions from the HDR test T31.5 are presented. The last part discusses different ways of recombiner modeling during 3D distribution simulations and gives the results from validation calculations for the HDR recombiner test E11.8.1 and the Battelle test MC3. The results demonstrate that field code simulations with computer codes like GASFLOW are feasible today for complex containment geometries and that they are necessary for a reliable prediction of hydrogen/steam distribution and mitigation effects.

1. Introduction

Since 1994 revised German safety regulations for new nuclear reactor power plants require the demonstration that hydrogen release during core melt down accidents in pressurized water reactors cannot generate combustion loads that cause early containment failure. For this reason, a strong need exists for reliable simulations of the major physical processes that control hydrogen behavior. One important item is the simulation of hydrogen distribution in the containment that includes the effects of various mitigating measures such as inertization, catalytic recombination and early ignition of burnable mixtures. GASFLOW is a 3D field code that allows the integral analysis of hydrogen distribution and mitigation measures including combustion in complex geometries of reactor containments during core melt down accidents. In 1994 Forschungszentrum Karlsruhe (FZK) obtained a basic version of GASFLOW from the Los Alamos National Laboratory in exchange for experimental data from German government funded programs in the Heißdampfreaktor (HDR) and Battelle Model Containment (BMC). GASFLOW has been extended since then at FZK to better describe specific needs for the simulation of hydrogen issues during core melt down accidents. Parallel to the model improvements large efforts have gone into validating GASFLOW with the new models. Code validation focussed on integral tests from the HDR and Battelle test program. We have reported earlier about our GASFLOW simulations of the steam hydrogen distribution test T31.5 and of the combustion tests from the HDR E12 and the Battelle HX series [1]. This paper summarizes the GASFLOW analysis of the HDR steam hydrogen distribution test E11.2 performed in the last year. It reports about scoping analyses to dilute burnable steam/hydrogen mixtures with different modes of CO₂ release and it presents the recent GASFLOW modeling and validation of catalytic recombiners during the steam hydrogen transport. The discussion is preceded by a brief overview of the GASFLOW code and the recent improvements made to this code.

2. Overview of the GASFLOW code

GASFLOW is a 3D finite-volume computer code, that solves the time dependent, compressible Navier-Stokes equations for multiple gas species. The fluid dynamics algorithm is coupled to the chemical kinetics of combusting liquids or gases to simulate diffusion or propa-

gating flames in complex geometries of nuclear containments. Fluid turbulence is calculated to enhance the transport and mixing of gases. The basic GASFLOW version 1.0 [2] has been improved by adding a homogeneous two phase model for steam/water mixtures, by including a film tracking model for steam condensation and vaporization on structures, and by a new heat conduction model for a reliable prediction of the structure surface temperatures. The code structure was generalized, different code versions that existed for transport and combustion simulations were merged so that the homogeneous equilibrium two phase model can now also analyze sequences of transport and combustion processes. This involved temperature dependent analytic function approximations for steam and water in the form of higher order polynomials. The functional format for the steam/water properties was made consistent with the existing gas component library that was developed on basis of the JANAF tables [3] and results from the Chemkin code [4]. The approximations were made to the water/steam function library from Thurnay [5].

An ignitor model was integrated into GASFLOW. It allows to position up to 300 ignitors (glow plugs or spark plugs with a given spark frequency) in the containment. A time step logic reduces and increases the time step between transport and combustion phases.

The recombiner model in GASFLOW simulates reaction rates as function of local hydrogen/oxygen concentrations around the recombiner with inclusion of all component diffusions. Correlations from Siemens and from GRS [6] can be applied. Use of these correlations requires a detailed simulation of the local geometry particularly in the startup phase with instationary convections. The development of the buoyancy driven convection can be quite complicated and involve flow reversal when hydrogen stratification initiates higher concentrations above the recombiner. For this reason we have implemented also a coarser model for the recombiner. It simulates the flow through the recombiner box as an active ventilation process with a velocity boundary condition applying local experimental information from qualification tests for the recombiner (see also part 3.3.2).

The newly implemented ignitor and the recombiner model allow integral GASFLOW analyses that simulate transport and combustion sequences with inclusion of all important mitigation measures (in particular the so-called "dual concept" with ignitors and recombiners currently under discussion for German nuclear power plants).

A general procedure for maintaining and updating GASFLOW in a multi-user environment has been developed. All updates are made to a generic code version. Automatic procedures (Makefiles) then generate platform dependent source codes from this generic version, compile these versions and generate executables for debugging and production runs. Current platforms on which GASFLOW runs are the IBM Risc6000/590, the SGI machine with the new R8000 processor, the Cray J90 and the mainframe machine Siemens/Nixdorf S400 in Darmstadt.

3. Code Application

The status of the GASFLOW code will become clearer when discussing results in the different application areas of this code. We will report here only about our validation and application analyses for steam/hydrogen distribution. This includes results from simulations of mitigation measures with CO₂ dilution and from simulations involving catalytic recombiners. Steam/Hydrogen transport simulations with/without local combustions at ignitors have also been performed. They will be presented in a separate paper of these proceedings [7].

3.1 HDR Test E11.2

The successful analysis of the steam/hydrogen distribution test E11.2 [8] in the Heißdampfreaktor (HDR) was a major achievement in our GASFLOW activity in 1995. This HDR test was characterized by a strong hydrogen stratification in the dome. Until today no lumped parameter code could give a satisfactory description of this stratification that results from an

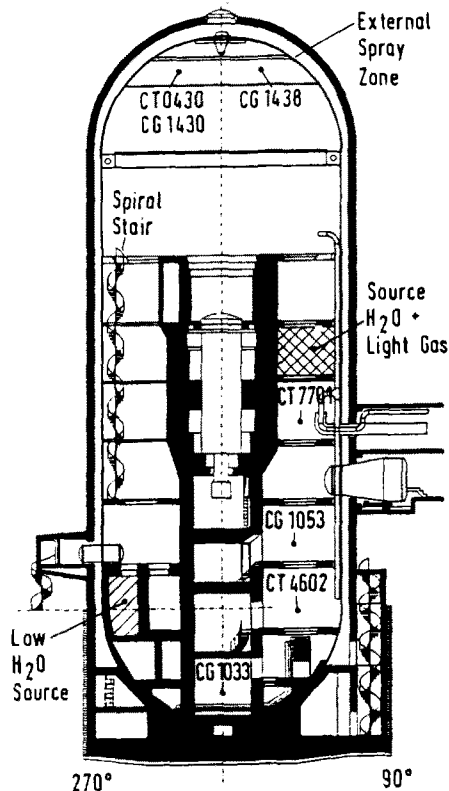


Fig. 1: HDR Containment for E11.2 external spray cooling.

The initial steam injection (Fig. 2) comes from an external source and from a blowdown source that produce 109 tons of steam over a 12h period. The light gas release initially overlaps with the steam release. It adds 151 kg of hydrogen simulant over a 1h time period. The release position in our analysis is the same as for the upper steam source. A steam release from the lower location starts after the hydrogen release at 13h. It adds another 23 tons of steam over 3h. The outside spray cooling on the spherical part of the steel shell is activated from 16 to 20h. We simulate the spray cooling by specifying a time dependent temperature on the outside of the steel shell over a top area that corresponds to the spherical part of the containment shell. The specified spray temperature has been determined from a least square fit to all measured outside temperatures of the spray cooled part of the dome. The spray temperature decays from 80 C down to

H₂ release at a rather high position in the containment [9]. Our GASFLOW simulation of test E11.2 is the only field code simulation of this experiment to date. On the first full GASFLOW field simulation, a much better stratification behavior was calculated than with the lumped parameter codes. We have analyzed test E11.2 with strong support of Siemens/Nixdorf in Munich who gave us free computer time on their S400 mainframe computer in Darmstadt. This computing power was necessary for the analysis of the long problem time (20h) in the complex HDR containment model with 12000 computational cells.

The HDR containment (Fig. 1) is 60 m high and 20 m in diameter, it has a free gas volume of 11300 m³. Its geometry is quite representative for commercially sized reactor containments. There is a gap between the steel shell and the outer concrete shell that is characteristic for German PWR's of the Konvoi type. The HDR test E11.2 simulates steam hydrogen release after a small break loss of coolant accident (LOCA). It starts with a steam release at a high position that is followed in the same room by the release of hydrogen in the form of a light gas (85% Helium, 15% Hydrogen). Then further steam is released in the lower part of the containment. A water spray on the spherical part of the steel dome finally simulates an

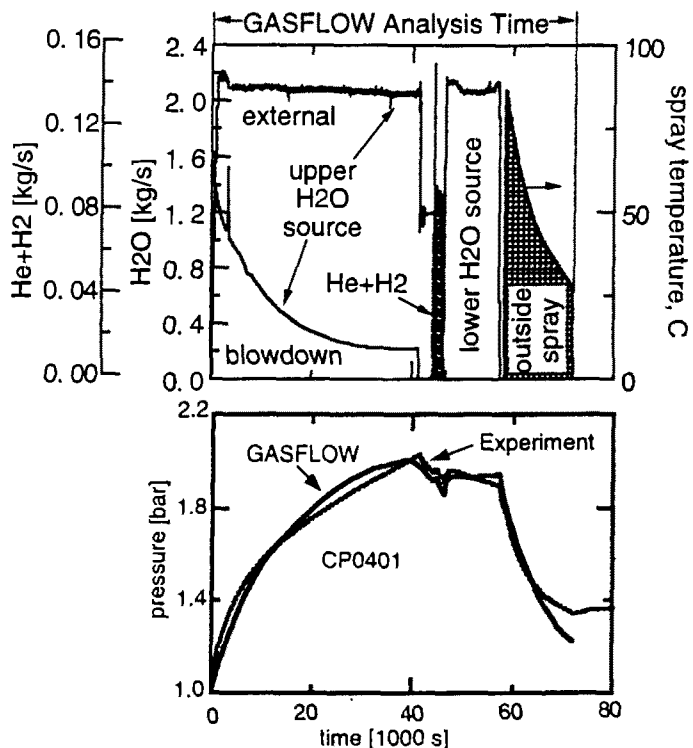


Fig. 2 Source and pressure for HDR test E11.2

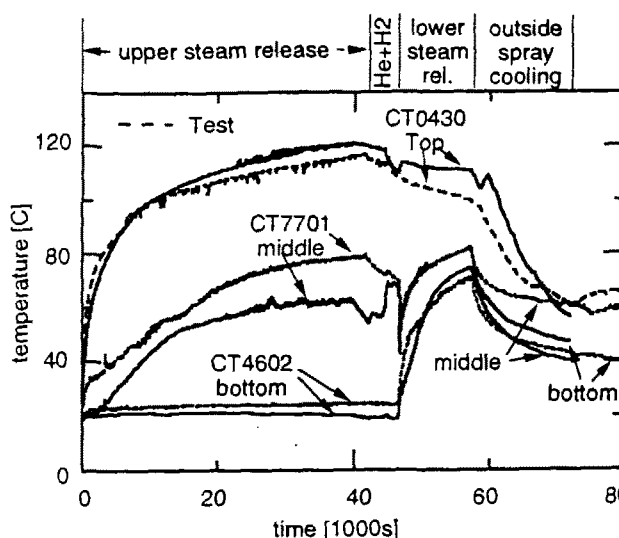


Fig. 3: Temperatures in HDR test E11.2

opens up nearly all the surface of the steel shell for steam condensation.

The calculated temperature at the top of the dome (Fig. 3) peaks 5 degrees higher than the test data at a maximum of 120 C. In the final phase it drops too fast due to some overprediction from the simplified representation of the spray cooling. This becomes also visible in the pressure. In the bottom part of the containment our calculated temperatures are 5 degrees below the measured data. Their increase during the lower steam release phase is quite well pictured. The enthalpy of the condensate running down the walls from the upper release location has been neglected in GASFLOW. Therefore the calculated gas temperatures prior to the light gas release phase are significantly (up to 30 degrees) lower than the measured ones in the middle section of the containment below the source room. This overestimates gas densities in these regions. When the steam release starts at the lower location this reduces the upward relocation of light gas that was initially dispersed downward.

The initial hydrogen stratification after the light gas release is quite well predicted (Fig. 4). The light gas stratification also continues during the phase with the low steam release. But because of the before discussed problem with the gas temperatures less previously downward convected light gas is convected upward with the steam release in the lower part of the containment. Sensitivity studies show that with higher gas temperatures in the middle part of the containment this stratification effect during the lower steam release phase would be more pronounced. Note however, that GASFLOW predicts a continuing light gas stratification also during the low steam release. Lumped parameter codes don't show this [9].

The outside spray cooling after 16h increases the hydrogen volume fraction initially by condensing the steam. Convection then superimposes and causes mixing processes which decrease the light gas volume fraction in the second half of the spray cooling phase. GASFLOW describes these processes quite well. The calculated light gas volume fraction at the top of the dome peaks at 18%, the measured one near 23%. Most of the differences to the test data result

25 C over a 4 hour period. Our GASFLOW analysis covers a 20h time period (72000 s) and extends to the end of the external spray cooling phase.

The calculated pressure (lower part of Figure 2) matches the measured data quite well. The pressure transient reflects the above pictured different injection and cooling periods. Both curves peak around 2 bar. It is only in the final phase where the calculated pressure decays faster than in the test. The good agreement in the pressure data was obtained only after following the recommendation from GRS to simulate a gap between the internal concrete structures and the steel shell. Such gap did indeed form after the numerous earthquake tests performed in the HDR containment. It

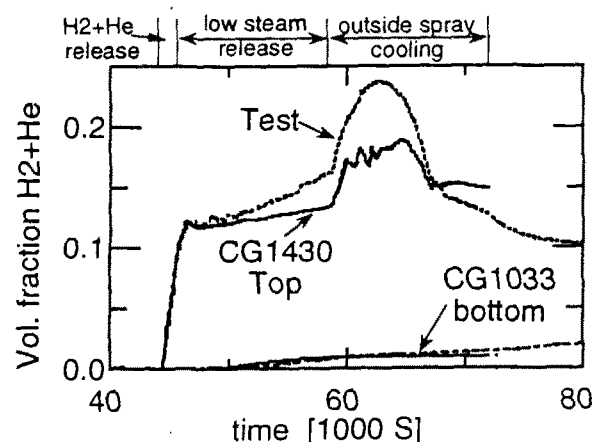


Fig. 4: Hydrogen distribution in HDR test E11.2

from too little stratification during the low steam release phase. The reduction of the light gas content by the mixing process occurs somewhat later than in the test. In the late phase it is overshadowed by a too strong spray cooling effect. Very good agreement with the measured data is obtained in the low region of the containment. Lumped parameter codes tend to overpredict mixing causing too high values in the lower part of the containment.

This is demonstrated in the comparison of our results to published data [9] from open post test predictions with a number of lumped parameter codes (Fig. 5). The sensor locations for this comparison refer to locations in the top and bottom part of the containment (Fig.1). Note the differences in the data reading of the top H2 sensor which is at the same axial position only a short distance

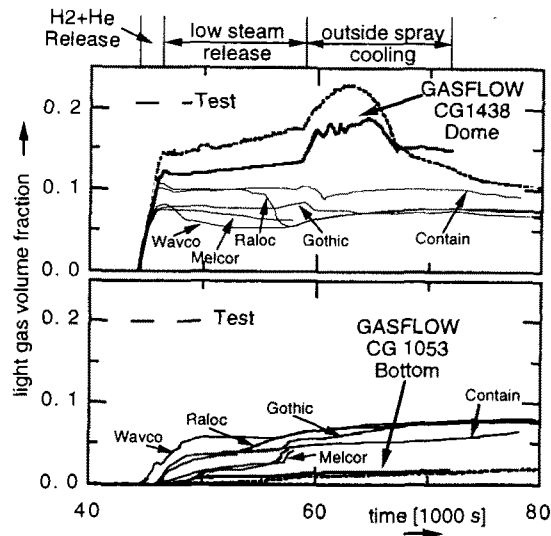


Fig. 5: GASFLOW vs. lumped parameter code results for H2 distribution in test E11.2

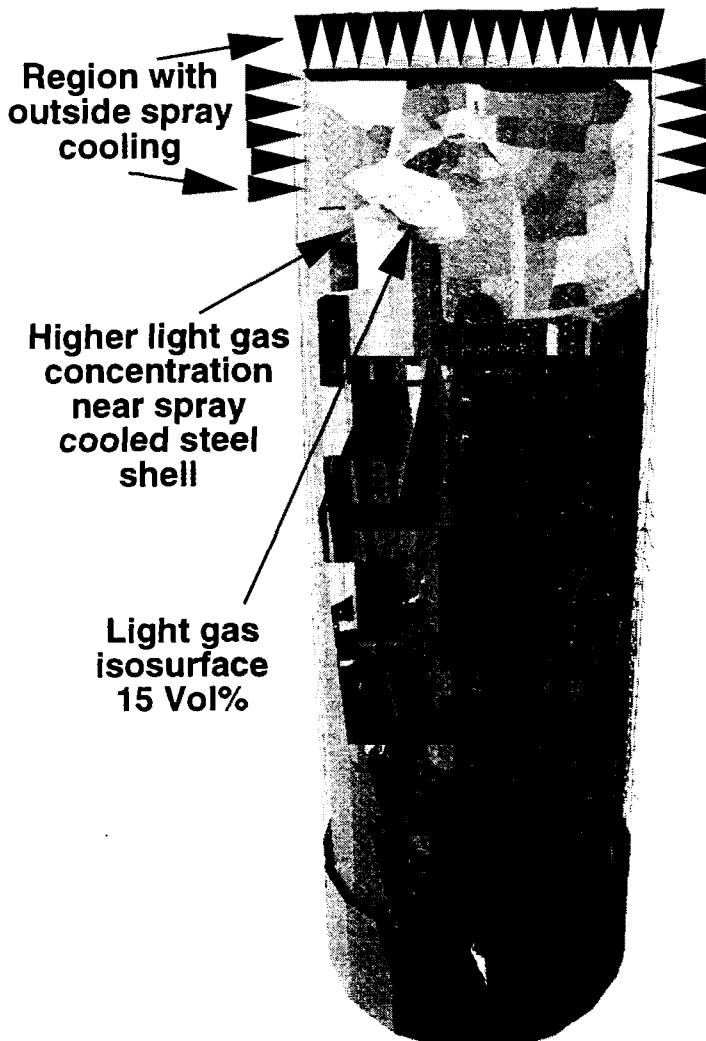


Fig. 6: Local light gas enrichment near spray cooling surface in HDR test E11.2

away from the one in Figure 4 while GASFLOW predicts roughly the same concentrations at both top locations. Compared to the GASFLOW results the light gas volume fractions from the lumped parameter codes are lower at the top and higher at the bottom with only a weak concentration gradient towards the top. The lumped parameter codes predict a more mixed configuration already right after the hydrogen injection. They don't predict any stratification during the low steam release phase. The light gas volume fraction increase during the spray cooling phase is also not predicted by the lumped parameter models. A nearly instantaneous convection process must occur in the lumped parameter models and smear out the light gas enrichment during the spray cooling phase.

The outside spray cooling enriches the light gas mainly next to the cooled walls and below where most steam condenses. This becomes visible in Figure 6 which gives a cut through the HDR containment from the CAD code Kismet [10]. We have displayed the 3D isosurface of 14.8% for the light gas volume fraction into

this cut. The arrows mark the region where the outside steel shell is given the measured temperature (Fig.2) during the spray cooling phase. The picture documents the strong light gas stratification in the dome and the strongly three dimensional effects due to the local light gas enrichments which are not captured with the lumped parameter models.

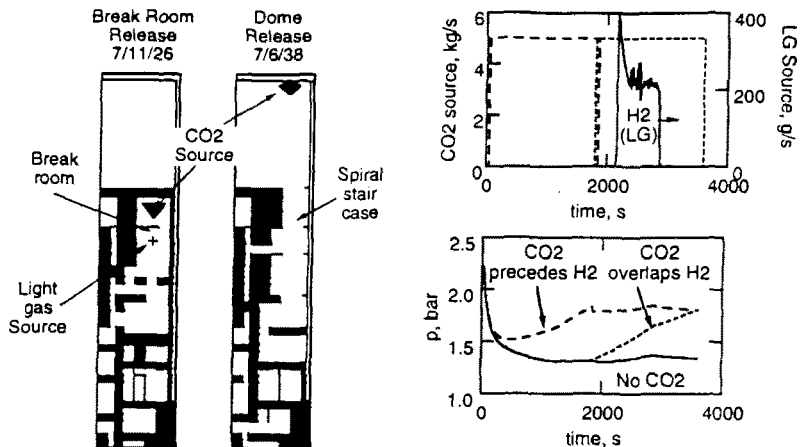


Fig. 7: Hydrogen mitigation with different modes of CO₂ inertization/dilution under conditions of HDR test T31.5

3.2 Analysis of dilution effect from CO₂ injection

As seen in HDR test E11.2 there is a certain potential for hydrogen stratification leading to flammable or even detonable gas mixtures in the dome. The potential of such hydrogen/steam/air mixtures for a deflagration can be greatly reduced and in the limit even avoided, if the containment atmosphere can be diluted in time through release of an inert gas like CO₂. We have made scoping analyses for the inertization effect of CO₂ under these conditions and with the geometry of HDR test T31.5 [1].

We injected 9 tons of CO₂ into the HDR containment over a time period of 30min. The CO₂ amount was not enough to fully inertize the containment, it could rather dilute the hydrogen concentration to below the detonation limit. We investigated two release positions for the CO₂, one at the top of the dome, above the spiral stair case of the HDR containment, the other in the original break-room above the release position for the hydrogen (Fig. 7). Two release intervals were simulated, a release prior to the hydrogen release, and a release that overlaps with the hydrogen release. In all cases the 9 ton CO₂ release increased the pressure by about 0.3 bars compared to the case without CO₂.

Figure 8 shows the mixture developments in the break-room and in the dome region. Plotted

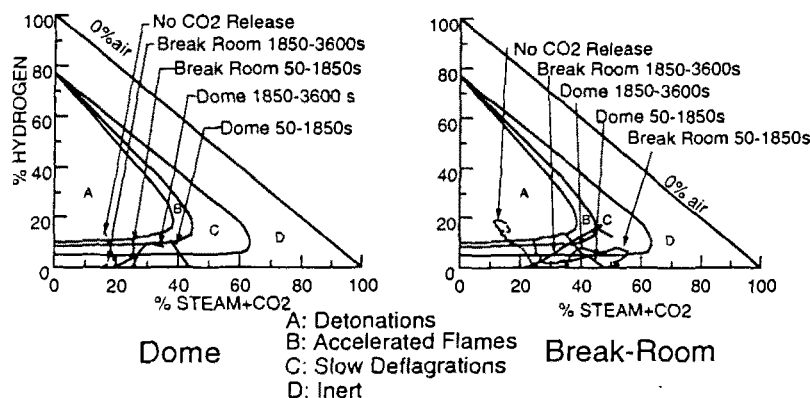


Fig. 8: Flammability potential with different modes of CO₂ dilution

are the hydrogen versus the inert gas concentrations (i.e. the sum of the steam and CO₂ volume fraction). Displayed into this figure are the flammability maps for hydrogen steam air mixtures. Assuming that CO₂

effects the flammability in the same way as steam (this needs still confirmation through experiments) one can see that the 9 tons CO₂ release increases the inert gas volume fraction enough to avoid the dangerous regime for detonations. The simulation without CO₂ dilution on the other hand enters the regime of detonable mixtures. The best results are obtained with a CO₂ release that overlaps the light gas release. In this case the downward flow of the heavy and the upward flow of the light gas lead to a rather homogeneous mixing. CO₂ released prior to the light gas still acts as a strong dilutor, but it cannot prevent stratifications. Overall the high release position for the CO₂ is more favourable particularly when the CO₂ release occurs prior to the light gas release. The high release position for the CO₂ assures an atmosphere with inertizing CO₂ in the dome region where hydrogen stratification builds up the strongest combustion potential. The sedimentation of the heavy and stratification of the light gas during the CO₂ inertization can be well pictured in the 3D field approach from GASFLOW. It may be good to validate the distribution simulation with real CO₂ distribution tests which should best cover similar conditions as those in HDR. Then GASFLOW can be used as a tool to design the CO₂ dilution to rule out turbulent deflagrations and detonations for a wide spectrum of accident conditions in real reactor containments.

3.3 Analysis of Recombiner Tests

There are three aspects which come into play when looking at catalytic recombinations of hydrogen and oxygen during different release scenarios. One is the local chemistry at the recombiner surface, the other the diffusion and convection process of the reactants to the recombiner surface, and the third the overall convection processes in the reactor containment associated with the pumping action from the catalytic recombination energy. While the surface chemistry of the recombiner requires other models, GASFLOW simulates the remaining two aspects quite well, using defined reaction rates as function of local mixture composition or in a macroscopic approach flow rates with overall recombiner efficiencies as function of the gas composition at the recombiner inlet.

3.3.1 HDR test E11.8.1

Models for calculation of the local reaction rate in the fluid node adjacent to the recombiner surface have been implemented using correlations from Siemens and from GRS. The GRS correlation calculates the recombination rate in g hydrogen/sec by an Arrhenius type correlation from:

$$\dot{m} = N_{rec} \cdot k_1 \cdot (\text{mol}_h)_2^{1.05} \cdot e^{-\Delta E(T)/(R \cdot T)}$$

with $\text{mol}_h = \text{mol H}_2/\text{cm}^3$, k_1 recombiner constant

$\Delta E(T)$ = activation energy and N_{rec} being the recombiner surface

This model has been applied in the analysis of the HDR test E11.8.1 [6]. In this test that was specified and performed by

GRS a 0.4 m² catalytic foil with a Pd-Cu-Ni alloy on a SS-304 plate was excentrically positioned inside a spherical steel shell (Fig.9). The 10 m³ steel shell was

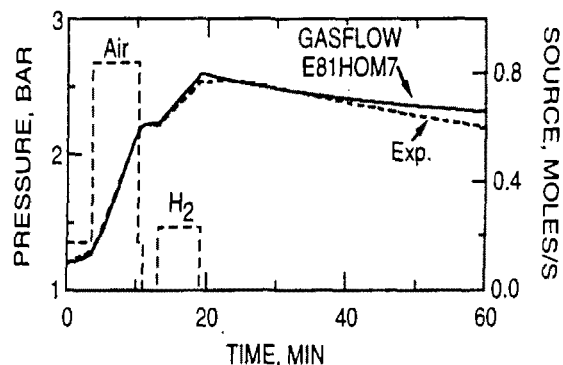
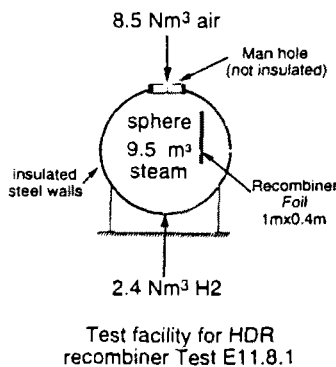


Fig. 9: Configuration and pressure for HDR recombiner test E11.8.1

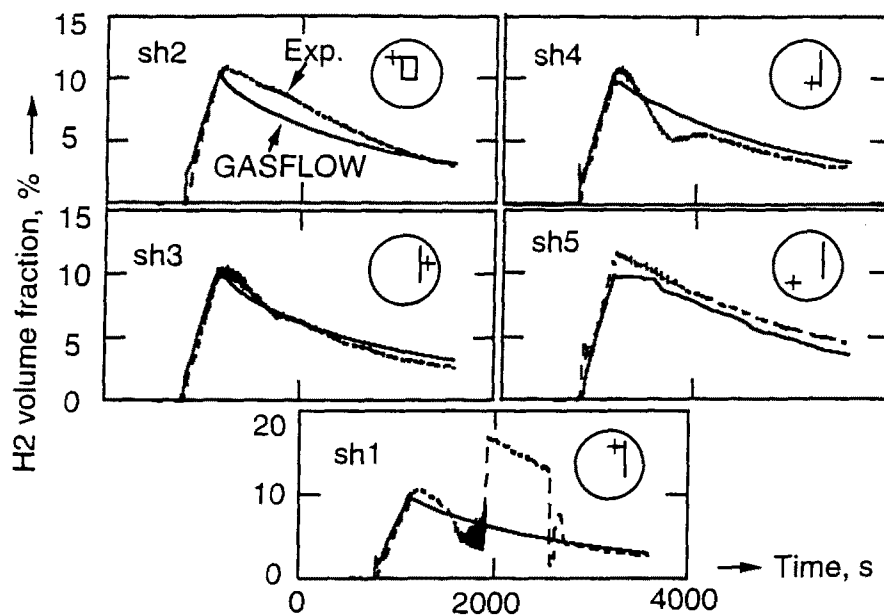


Fig. 10: H2 volume fractions in recombiner test E11.8.1

sian mesh applying a 180 degree symmetry. The simulation covered a 60 min period. It applied the above correlation for the local hydrogen recombination rate. The local use of this correlation from a lumped parameter approach in a 3D simulation required a readjustment of the coefficient k_1 but then gave quite good overall agreement with the test data.

The calculated pressure (Fig.9) agrees quite well with the measured data. It reflects the initial air and hydrogen injection. Its decay due to the reduction of the number of moles during the recombination is calculated somewhat slower than in the test which we attribute to the neglect of radiative cooling in our analysis.

The calculated hydrogen volume fractions (Fig. 10) match the measured ones quite well nearly at all locations. There is a slight tendency for an underprediction in the regions away from the recombiner (sh5 and sh2) indicating a suction effect of the simulated recombination with respect to hydrogen. The results are largely insensitive to reducing the mesh size adjacent to the recombiner foil from 10 to 1 cm. This is related to the excellent diffusion properties of hydrogen which allows to quickly replace the hydrogen consumption also in the smaller node adjacent to the recombiner. It did lead to higher local node temperatures because the same energy was released into a smaller node. We plan now to release the recombination energy into the outermost structure node of the recombiner foil instead into the fluid node adjacent to its surface. This will give a more precise description of the structure surface temperature which is also important to correct for radiation heat transfer.

The succesful analysis of test E11.8.1 shows, that convection and diffusion processes can be adequately accounted for in this detailed GASFLOW simulation of the recombiner foil.

3.3.2 Battelle Test MC3

Models with local reaction rates can be directly applied to the simple recombiner foil geometry from test E11.8.1. Their application to a full recombiner box in a complex containment is more difficult. It may require the calibration of the coarse 1D model for the recombiner box that is integrated into the containment model with a more complex 2 or even 3D model that simulates only the complex chemical and thermohydraulic processes and their heat capacities inside the recombiner box. Fortunately such refined model is only necessary for the detailed simulation of the start up phase, when the pumping action of the recombiner develops under the competing effects of the energy release from the recombination and the energy absorption from the heat capacities of the recombiner structures. This has become clear during

insulated. It was initially flushed and pressurized with steam to a pressure of 1.2 bar. Subsequently it was filled with air from the top and hydrogen from the bottom. The pressure, the steam and hydrogen concentrations and the temperatures were measured at various locations inside this vessel during the hydrogen recombination. GASFLOW simulated this experiment in a 3D cartesian

Dome R9

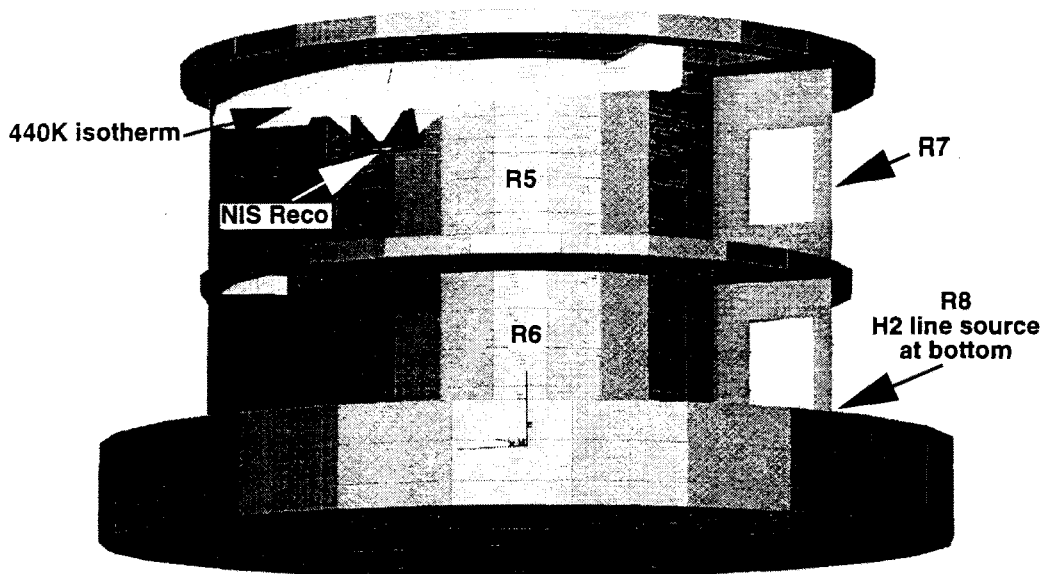


Fig. 11: Kismet Model of the BMC MC3 recombining test with hot plume from recombining the analysis of the Battelle recombining test MC3 [11].

Test MC3 involved the entire Battelle model containment with the four banana rooms and the dome with a total free gas volume of 640 m³. We developed a 3D cylindrical model for GASFLOW with a total of 4752 computational cells. Simulated were the injection and transport of hydrogen and the convection processes in a steam/air atmosphere with hydrogen recombination in a NIS type granulate recombining module [12]. A Kismet cut shows this test configuration (Fig. 11). The outer walls of the banana rooms have been intentionally left out in Fig. 11 also the outer containment walls and the top of the dome for display purposes. The containment has a diameter of 12 m and a height of 10 m. Hydrogen is injected from a line source at the bottom of the lower banana room R8. The hydrogen finds its way into the banana rooms above where it stratifies near the ceilings. The NIS recombining box is located in the banana room R5 on the opposite side of the source room in the upper level. Initially it sees a somewhat higher gas concentration at the top than at the bottom.

We attempted to model the startup of the recombination and pumping process in this box together with the convection in the whole containment in a coarse single node model using local recombination rates and heat capacities. Our model predicted an initial downward flow through the recombining box with flow reversal occurring after structural heat up from the recombination. But it could not come to a correct prediction of the quasistationary flow rates and recombination efficiencies which had been measured. The experiment was analyzed for over 4 hours and actually showed only a relatively short startup phase during which the pumping action developed.

A much better agreement was obtained after we implemented a correlation for the volumetric flow rate through the recombining box which had been developed by Fischer [11] as function of the hydrogen volume fraction at the recombining inlet. This correlation was developed directly from the local measurements at the recombining inlet and outlet. It applies a steady state volumetric flow Q_0 given by

$$Q_0 = aC^b$$

where

C = hydrogen volume fraction

$$a = 0.67e+06 \text{ (cm}^3/\text{s)}$$

$$b = 0.307$$

are all determined from the experimental data. The time-dependent behavior of the volumetric flow $Q(t)$ is described by the differential equation

$$\frac{dQ}{dt} = \frac{1}{\tau}(Q_0 - Q)$$

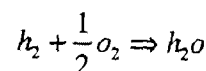
where τ is a relaxation time determined by the experiment to be 1800 s. By carefully examining the time averaged data at the recombiner inlet and outlet, Fischer found that the hydrogen recombination through the flow passages was incomplete. He determined an efficiency factor

$$\eta = 1 - C_{\text{outlet}} / C_{\text{inlet}}$$

where $\eta = 0.846$. GASFLOW has been modified to automatically formulate this volumetric flow rate for the given hydrogen volume fraction in the node below the recombiner entrance as a velocity boundary condition (like an active ventilator) at the entrance to the recombiner box. Using a constant efficiency η of 0.846 GASFLOW simulates a recombination rate from this inflowing hydrogen as

$$\dot{R} = \eta \cdot Q \cdot \rho_h$$

We use this recombination rate to drive the chemical kinetics



and release energy into the recombiner region with a limitation imposed by the availability of oxygen. We found that this model was adequate to represent the quasistationary convection processes in the reactor containment associated with the pumping action from the catalytic recombination energy during test MC3.

The results from our GASFLOW analysis of test MC3 are shown in Figure 12. The unwrapped containment geometry shows the paths which the hydrogen can take on its way from the source to the recombiner box. The total H2 release was 3.5 kg in this test. It was released in two periods each of one hour with a one hour interval between the first and the second injection phase. The GASFLOW calculated hydrogen mass in the containment never exceeds 1 kg indicating that the recombiner is quite efficient in limiting the hydrogen accumulation inside the containment. This is also reflected in the hot plume above the recombiner that is displayed in Fig. 11 as the 440K isosurface and that imposes quite significant thermal loads on the ceiling. Most of the released hydrogen convects to the far end of the source room from where it is driven upward by buoyancy and sucked horizontally to the catalytic module in the adjacent banana room. The GASFLOW calculated velocity vectors in Fig. 11 (their origin is at the ti-

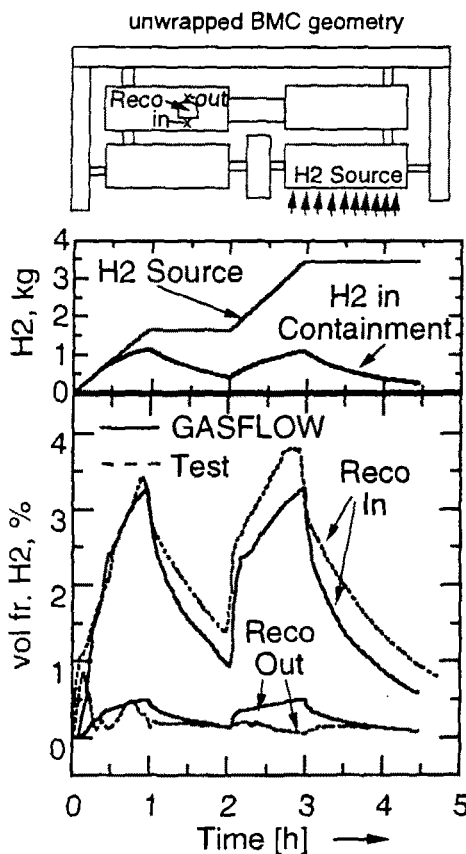


Fig. 12: H2 release and concentration at recombiner box in BMC test MC3

ny white dot their length represents their magnitude) document this convection. The time evolution of the hydrogen volume fraction at the recombiner inlet and exit is in good agreement with the measured data (Fig. 12) considering the large distance between the source and the recombiner through which the released hydrogen has to convect and diffuse. The Fischer model lacks some details in the startup phase (first 20 min) where it gives a somewhat too early start of the buoyancy pump. But the long time processes are the more important ones when recombiners are involved. For these processes overall this analysis demonstrates well the ability of GASFLOW to simulate hydrogen transport processes in large containment geometries in an integral approach that includes the pumping action and the species changes due to the recombiner box and that applies correlations for the recombiner that have been qualified with local test data.

The use of the locally measured volume flow rates and the measured efficiency of the recombiner under quasistationary conditions also allows to represent this recombiner in a coarser mesh than its actual geometrical dimension. Heat capacity effects of the structures but also of the gas inventory inside the recombiner box should then be negligible, all that counts is the volumetric flow through the box and the added recombination energy for the given efficiency. Test calculations show that the results in Fig. 12 don't change with a 50% increase of the recombiner area over its original size.

We have also started to look at Battelle tests from the GX series in which a Siemens recombiner was tested in a multi room geometry. It is intended to simulate this recombiner also based on the Fischer model. We have already derived quasistationary volumetric flow rates from the test data, but must still derive an appropriate time constant that helps to match the transient flow variations during changes of the hydrogen concentration.

Parallel to the validation analysis for various recombiner modules we are now testing also how a number of such recombiner boxes would operate in a real reactor containment and whether they could influence each other. For this purpose we have modeled the inner part of a German PWR containment with a height of 28 m and a free gas volume of 19000 m³. Our glass model for this containment from Kismet shows the 4 steam generators, the primary coolant pumps and the pressurizer and pressurizer blowdown vessel (Fig. 13). GASFLOW applies 22400 computational cells for this geometry. The steam generator towers are assumed to remain sealed at the top in this test calculation. Our recombiners are positioned

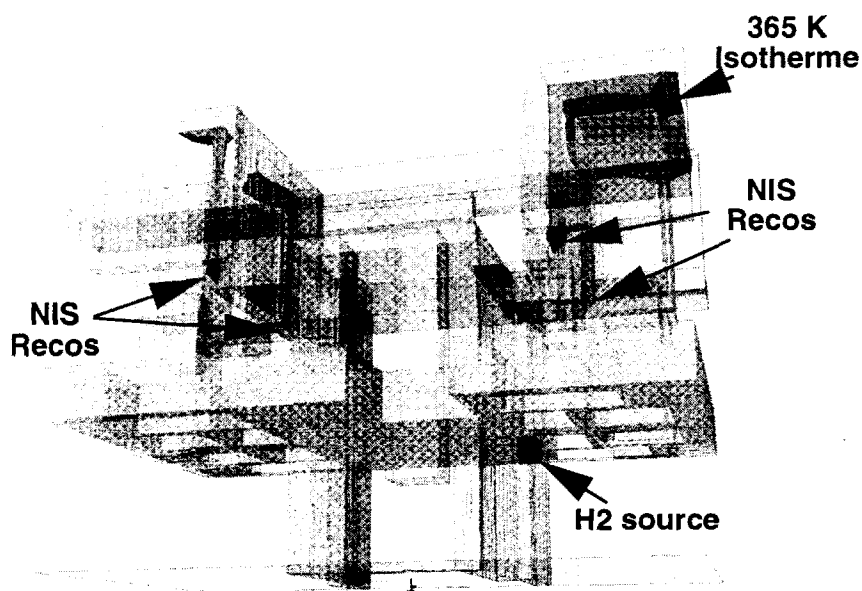


Fig. 13: 3D GASFLOW simulation of hydrogen recombination in the inner rooms of a German 1300 MWe PWR

symmetrically at the entrance to the steam generator towers. The source is excentrically located and simulates a rather mild steam/hydrogen blowdown after a rupture of one of the primary coolant pipes. GASFLOW simulates the full sequence of the steam/hydrogen release, the gas transport and the hydrogen recombination. The hot plume above the recombiners is characterized here by the 365 K isotherme and reflects the asymmetry of the source. Our results show that in principal it is feasible to use GASFLOW to determine the number and location where such recombiners should be put to be most effective in different release scenarios.

4. Conclusions

Significant progress has been made in the development and application of the GASFLOW code. We are now able to simulate a wide spectrum of mitigation measures in a consistent 3D approach with the distribution analysis. The distribution analysis of steam hydrogen mixtures with GASFLOW has been successfully validated with the analysis of two major HDR tests (T31.5 and E11.2) on basis of a condensation model which was validated with the analysis of the containment thermohydraulics of the Phebus FPT0 test [13]. We have used the same modeling to do integral analysis for the European Pressurized Reactor (EPR) [7]. The presented application/validation analyses involved distribution simulations with the two major mitigation mechanisms inertization/dilution and catalytic recombination. We have successfully validated different models for recombiner simulation. Validation efforts are continuing. Containment inertization/dilution by CO₂ still lacks appropriate experimental data for the validation, but such tests are in discussion now, also experiments that specifically explore the flammability limits and combustion processes of air/CO₂/steam/hydrogen mixtures.

Our results have shown, that field code simulations have become feasible and provide reliable prediction of both hydrogen/steam distribution and integral simulation of transport and combustion processes. The hydrogen stratification and local hydrogen enrichment during spray cooling in HDR test E11.2 documents these needs impressively. Lumped parameter codes can be adjusted and calibrated to include relevant findings from the field codes. Field code simulations that are now possible for complex containment geometries should be seen as a help to overcome the remaining uncertainties in the predictions of the lumped parameter codes. GASFLOW demonstrates that field codes can directly help with the design and optimization of mitigation systems. GASFLOW can be used to determine number, position, and size of recombiners and their possible interactions, to determine number, position, and spark frequency of ignitors, and to specify sources for gas dilution over a spectrum of hydrogen release scenarios. But the computer time requirements for these analyses are significant, the computer running time in the analysis of test E11.2 was about 5 times the analyzed problem time on the fastest available machine, the Siemens Nixdorf S400. We will further improve on the performance after implementing a new Poisson solver that will allow a more effective parallelization of GASFLOW. Also the development of geometry models for large containments needs to be made easier. We plan on improving this with the development of a graphical user interface for GASFLOW on basis of existing software to set up complex geometry models for our detonation codes.

5. Acknowledgments

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2.3 GOTHIC ANALYSIS OF POST-ACCIDENT HYDROGEN MIXING BEHAVIOUR IN CANDU FUELLING MACHINE VAULT

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Abstract

The GOTHIC code was used to assess the post-accident hydrogen gas mixing patterns in a CANDU reactor containment and demonstrate the acceptability of Ontario Hydro Nuclear's hydrogen mitigation methods. The fuelling machine vault, being a small volume room containing major reactor piping, is the room of most concern with respect to hydrogen concentrations. Detailed three dimensional modelling of the gas mixing patterns in the fuelling machine vault was completed. Results showed that, even without forced air circulation, there is enough dispersion of hydrogen to other parts of containment to preclude the build-up of sensitive mixtures in the vault. For a brief time during the peak period of hydrogen release, hydrogen concentrations rise to close to the lower ignition limit in a small portion of the vault, but these hydrogen-steam-air mixtures are considered acceptable. Natural mixing alone is sufficient to preclude damaging hydrogen burns.

Introduction

Severe reactor accidents can result in significant hydrogen releases to the containment atmosphere. In multi-unit CANDU reactor stations, provided there is adequate mixing, the total containment volume of about 315,000 m³ is sufficient to preclude hydrogen concentrations high enough to give rise to damaging burns. However, the containment design includes a number of rooms in which higher local concentrations of hydrogen may occur. The fuelling machine vault is the room of most concern since it is of relatively small volume (3500 m³) and contains the major reactor piping (inlet and outlet headers, and feeders), as shown in Figure 1a.

In order to demonstrate the acceptability of the hydrogen mitigation measures at Ontario Hydro Nuclear stations, we have completed detailed three dimensional modelling of the gas mixing patterns in the fuelling machine vault using the GOTHIC code. The results confirm that, with or

with forced air circulation, local concentrations of hydrogen gas are not sufficient to give rise to damaging burns.

Background

To preclude damaging hydrogen burns, Ontario Hydro adopted a Hydrogen Ignition System in its multi-unit CANDU reactor containment to mitigate the consequences of hydrogen burns. Glow plug igniters were placed in various containment rooms including the fuelling machine vaults (Figure 1b). The number and location of igniters were selected based on engineering judgement in the absence of detailed modelling. These igniters would automatically initiate a burn under accident conditions as soon as hydrogen reaches its lower ignition limit, thereby preventing further accumulation of hydrogen in the containment atmosphere. The ignition limits for hydrogen-air-steam mixtures initiated by a glow plug igniter are shown in Figure 2. The lower ignition limit is defined by 5 volume percent of hydrogen in dry air, and 15 volume percent of hydrogen in 55 volume percent steam. Mixtures of 5 volume percent inside ignition limit (see Figure 2) do not result in damaging hydrogen burns.

Provided that there is adequate mixing, the 315,000 m³ volume of a typical CANDU reactor containment is sufficient such that there is little risk of a hydrogen burn. The containment design includes a number of rooms, however, in which higher local concentrations of hydrogen may occur before the hydrogen is adequately mixed throughout containment. The room of most concern is the 3500 m³ fuelling machine vault since it is a relatively small volume containing major reactor piping (Figure 1a).

Hydrogen mixing is an important component of the installed Hydrogen Ignition System, with igniters placed at fixed locations inside containment. The concern is that localized pockets of hydrogen may form temporarily if the rate of release is faster than fan induced or buoyancy driven flow can disperse it. Pockets of hydrogen can also form near the ceiling, if the release is near the ceiling since buoyancy driven flow is minimized and stratification develops. If stratification occurs, the fan induced flows may not be strong enough to penetrate the hydrogen layer, depending on the relative location and elevation of the ventilation intakes and exhaust outlets. A detailed assessment of the general mixing behaviour in containment, using the GOTHIC code, is required to demonstrate the adequacy of the hydrogen mitigation system.

GOTHIC [1] is a containment code consisting of a preprocessor, solver, and graphics modules. The program features a flexible noding scheme which allows users to perform lumped parameter or three-dimensional modelling. GOTHIC solves the conservation equations for mass, momentum, and energy for multi-component, multi-phase flow, including non-condensable gases such as air and hydrogen. To assist in visualizing the gas mixing patterns, we created a GOTHIC plot data extraction program to postprocess GOTHIC graphics data and used a commercial three dimensional data visualization program called TECPLOT.

A detailed mixing calculation using a three dimensional (3-D) code for a complex containment configuration is a time-consuming task, so care is needed to select the cases for consideration.

The results of preliminary calculations [2] and mixing tests ([3],[4], [5]) have been evaluated and used to select the important cases for assessment. Mixing tests conducted by British Gas [3] showed that stratification is a concern for high elevation release of a buoyant gas, since the region of stratification was developed between the ceiling and the release point. A lower elevation release, therefore, allowed the gas to be more dispersed in the room compared to a high elevation release. The location of ventilation intakes and exhaust outlets was also shown to be important. If these are located below the stratified gas layer, the mixing will be minimal because the recirculation flow does not affect the layer of lighter gas near the ceiling. From a mixing perspective, therefore, the worst case is a hydrogen release near the ceiling.

In-situ mixing tests were conducted by the India Nuclear Power Corporation at its Kakrapar Atomic Power Project (KAPP) reactor [4], which has a containment configuration similar to Ontario Hydro's CANDU reactor containment building. In those tests, a buoyant gas was released into the fuelling machine vault. The gas tended to rise as a plume to the ceiling, but dispersed readily to the room above the vault. The degree of stratification was highest when the release was located closer to the vault ceiling. This is consistent with the observations from the British Gas tests.

Preliminary calculations [2] were performed using GOTHIC to assess the relative effects of rate of release and release location. As with the mixing tests, the worst stratified gas layer near the ceiling was formed if the release was also near the ceiling.

The selection of optimum cell size is an important user input parameter when using GOTHIC. Unfortunately, there is a trade-off between accuracy and practical computation times, where smaller cell sizes provide better accuracy at the expense of longer computing time. It was revealed in [2] that care must be taken in setting-up the grid size of the three dimensional GOTHIC model to ensure spatial convergence of the solution. Much finer cell sizes were needed when the release was rapid, or located near the ceiling.

A similar study was conducted by AECL Whiteshell Laboratories [5] where small scale gas mixing tests were simulated using GOTHIC. Their findings showed that a high elevation release resulted in larger concentration gradients in the stratified gas layer when compared to a low elevation release, and hence a finer modelling grid was required to simulate the test results. These calculated results, [2] and [5], are consistent with the British Gas and KAPP tests, and showed that GOTHIC is well suited for 3-D mixing calculations, provided care is taken in setting-up the model grid.

Methods

The selection of cases and the model set-up are described.

The fuelling machine vault is the chosen accident room, being a relatively small room with major reactor piping (as illustrated in Figures 1a and 1b). As such, there is a potential for a local accumulation of high hydrogen concentration before it disperses into other containment rooms

where it would be diluted and non-flammable. As shown in Figure 1b, openings in the vault are at the top (to a much larger room) as well as a floor opening (to a smaller room). Due to the similarity of the east and west vault, the east fuelling machine vault (room R-209 in Figure 1b) is arbitrarily chosen as the representative vault.

Based on our evaluation of the British Gas and KAPP tests, as well as our preliminary GOTHIC calculations, a representative case of a rapid release at high elevation is selected as potentially having poor mixing. A hydrogen release of 70 kg is assumed over a period of 3000 seconds, reaching a maximum release rate of 0.07 kg/s at 850 seconds. As revealed in [2], this is also more of a computational challenge, since a finer cell nodalization is required for spatial convergence of the solution.

In preparing the model, the detailed geometry of the fuelling machine vault is first examined. The vault is a fairly open volume, allowing the movement of the on-power fuelling machine, except at the upper level where the feeder cabinet is located. The volumes occupied by the upper feeder cabinet, containing large reactor piping (shown in Figure 1a), are modelled as "null cells" which are blocked out of the main volume. Junctions with estimated thermal hydraulic flow losses are used to model the openings at the top of the vault, including the 52 m² of walk-on panels and 3 m² of blow-out panels (see Figure 3). The floor level opening of 18 m² to room "R-109" is also modelled (see Figure 1b). Walkway platform gratings are modelled as flow resistances within the volume.

Under accident environment, natural convective flows resulting from temperature differences between the atmosphere and hot/cold surfaces affect mixing. Therefore, heat sources, from equipment heat load, and structural heat sinks, such as concrete and steel surfaces, are also modelled. Forced convective flows resulting from fuelling machine vault fans also affect mixing. There are four air cooling units (ACU) in the fuelling machine vault located 3.5 m above the floor (see Figure 1b). These fans provide localized mixing, each with a designed fan flow rate of 18.9 m³/s. One of these ACU fans is modelled in order to assess the independent effects of a fan on gas mixing patterns. The fan closest to the south wall of the vault is chosen as the operating fan since this fan has the least influence on mixing due to its location (see Figure 1b).

A varying grid size that best models the equipment layout in the vault, while also ensuring spatial convergence of the solution, needs to be considered. Thus, a computational grid size that caters for the most challenging case, with a release at the highest possible elevation, is selected based on the cell-size convergence study [2]. The 3-D grid used to represent the fuelling machine vault is shown in Figure 3. The general cell size is 4 m³. A finer cell size is modelled near the ceiling, about 1 m³. A total of 1134 cells are used to model the vault.

With the important cases and model nodalization selected, GOTHIC calculations are performed. The gas flow patterns and spatial distribution of gas compositions throughout the vault are evaluated. Based on the ignition limits shown in Figure 2, the presence of flammable mixtures are identified. Mixtures of gas composition that are 5 volume percent inside the ignition limit do not result in damaging hydrogen burns. Therefore, if gas mixtures in the vault do not exceed this 5%

criterion, the degree of mixing is considered adequate for acceptable performance of the hydrogen mitigation system.

Results

Results are shown for the two GOTHIC cases, a “no fan” case and a one fan operating case.

Figures 4 to 7 are velocity vector plots from the GOTHIC postprocessor showing the general flow patterns in a cutaway view of the fuelling machine vault. These graphs illustrate the gas flow patterns in the vault with respect to the release location and the various openings to other areas of containment. Figures 4 and 5 show results from the no fan case; Figure 4 showing results in the north portion of the vault and Figure 5 showing results in the south portion of the vault. Figures 6 and 7 show results from the one fan operating case. Figure 6 showing results in the north portion of the vault and Figure 7 showing results in the south portion of the vault

Figures 8 and 9 show a 3-D display of the spatial distributions of hydrogen and air (the remaining balance being steam) in the fuelling machine vault at 850 seconds, the time of peak release rate. These figures are snapshots produced by TECPLOT, a 3-D data visualization program. Figure 8 shows results from the no fan case and Figure 9 shows results from the one fan operating case.

Figures 10 and 11 summarize the gas distribution results compared to ignition limits. Figure 10 shows results from the no fan case and Figure 11 shows results from the one fan operating case. As part of the quantitative assessment, these plots display the calculated gas composition for all the computational cells representing the vault. The spatial density and distribution of the plotted points provide a good indication of the overall gas distribution.

Discussion

For the analyzed case with no mixing fans, the general flow pattern shows hydrogen dispersing readily upward to the ceiling and out to the room above the vault through top openings. The dispersion of gas to the room above the vault ensures that the gas concentration evolves gradually and continuously within the vault. This is very consistent with results from the British Gas and KAPP gas mixing tests, which gives confidence that GOTHIC is capable of giving sensible predictions on gas mixing patterns. Air ingress from the small room below the vault through the floor opening can also be seen. As shown in Figures 4 and 5, the gas flow patterns are symmetrical at the north and south part of the vault.

It is evident from Figure 8 that a hydrogen layer forms near the release location close to the ceiling. The stratified layer is not flammable, however, because of the relatively large portion of steam and insufficient air. Meanwhile, mixtures near the bottom of the room contain very little hydrogen (Figure 8), and so are also non-flammable.

Because this worst case of both a rapid and high elevation release leads to stratified gas layers, a period of time is needed to attain a well-mixed atmosphere. Nevertheless, no regions of sensitive mixtures are formed anywhere. Although a small portion of the vault contains mixtures that are up to 5% inside the ignition limit, they last for only a brief period (see Figure 10 at 850s). After the release rate slows down, hydrogen mixtures are below the lower ignition limit (Figure 10 at 2500s) due to the continuous dispersion of hydrogen to the room above the vault. There is no risk of a hydrogen burn after the release subsides. Since the atmosphere is acceptable at all times for this worst case, the installed hydrogen mitigation system is seen to be an adequate mitigation measure.

A single fan operating has also been investigated. Our findings indicate that a single fan has only a localized effect. As with the no fan case, it is evident from Figure 9 that a hydrogen layer forms near the release location close to the ceiling. The general flow patterns suggest that the fan provides circulating flows only in the south half of the vault, from the fan outlet elevation up to the elevation of the top openings (Figures 6 and 7). The fan flow does not penetrate the north half of the vault and the upper level, due partly to the location of the vents and the presence of the upper feeder cabinet. This is consistent with observations of the British Gas tests where some ventilation configurations (or fan locations) did not promote overall mixing. In the case of the fuelling machine vault with hydrogen release near the ceiling, fans have little effect during the period of maximum hydrogen release because recirculation flow is localized. After the release ends, the fans contribute to faster global homogenization, similar to the observations in the KAPP tests. The similarity of these calculations to test results (British Gas and KAPP) demonstrate that the code also gives sensible predictions on gas mixing patterns for fan cases.

As in the "no fan" case (Figure 10), there is a wide spread of hydrogen concentration within the vault; however, no regions of sensitive mixtures are formed anywhere. A small portion of the vault contains mixtures that are up to 5% inside the ignition limit, but they last for only a brief period (see Figure 11 at 850s). Thus, natural mixing (without fans) combined with dispersion prevents the formation of sensitive mixtures and no damaging hydrogen burns would occur.

Conclusions

The mixing of hydrogen in the CANDU fuelling machine vault, a room with burn potential, has been assessed. A limiting case was defined using findings from gas mixing tests conducted by British Gas and India Nuclear Power Corporation, and from some preliminary GOTHIC calculations. A rapid hydrogen release rate and highest release location, in the absence of recirculating fans, are considered having the highest potential for poor mixing. The effect of a recirculating fan on mixing is also examined. Based on the GOTHIC calculated spatial distribution of the hydrogen-air-steam mixtures, we conclude:

1. GOTHIC 3-D is well-suited for hydrogen distribution analysis, except care is needed to determine the optimum cell size for the detailed model to ensure spatial convergence of results with reasonable computation time.
2. A rapid high elevation release is the worst case because hydrogen tends to form a stratified gas layer at the ceiling during the period of release. A single fan does not significantly promote mixing for a high elevation release because only localized recirculation flows are established.
3. The hydrogen mitigation system is adequate because natural mixing and dispersion prevent sensitive mixtures from being formed in the fuelling machine vault during the period of hydrogen release. After the release subsides, there is sufficient dispersion through the top openings that there is no risk of a hydrogen burn.

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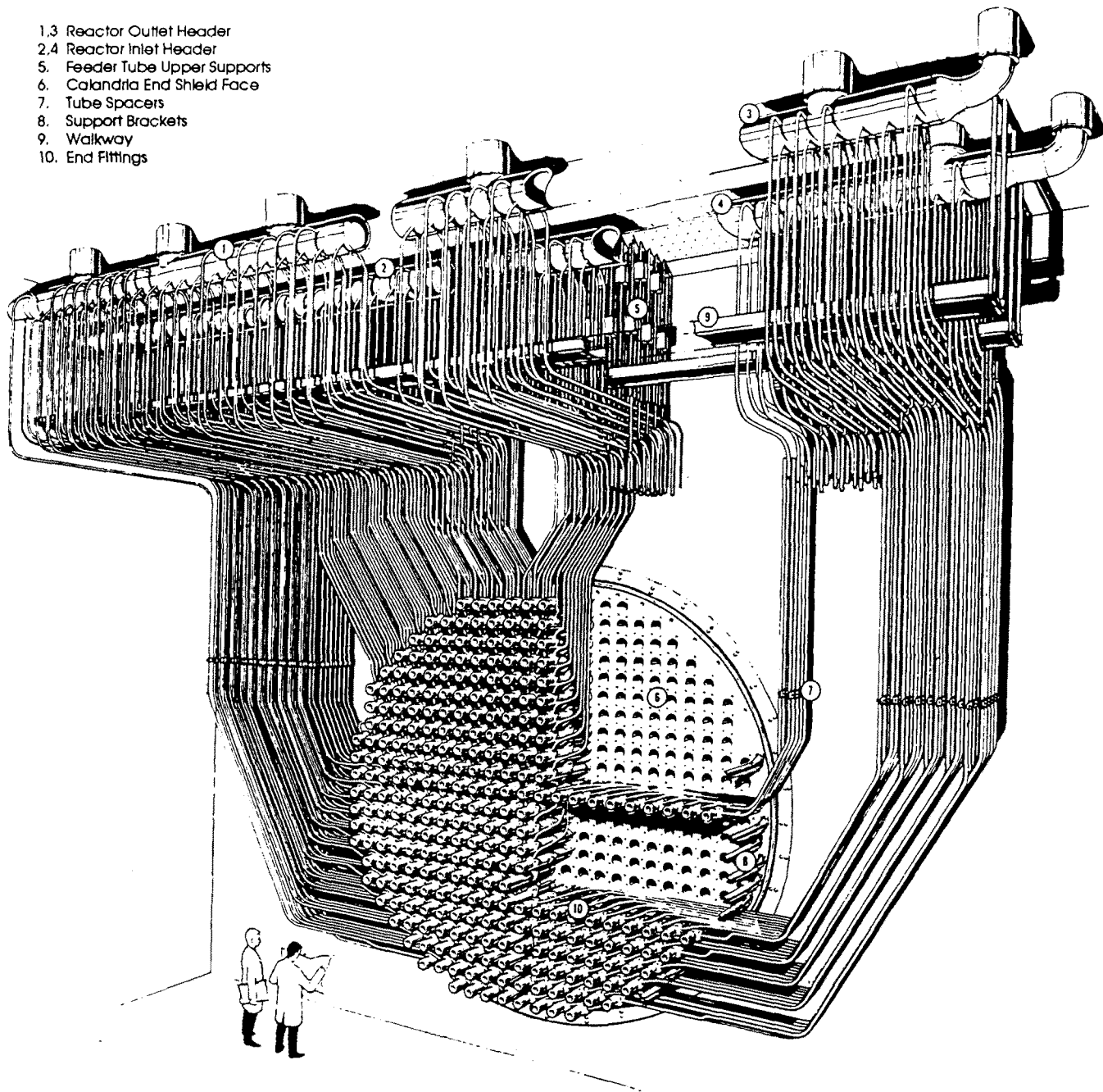


FIGURE 1a Major Reactor Piping Located In CANDU Fuelling Machine Vault

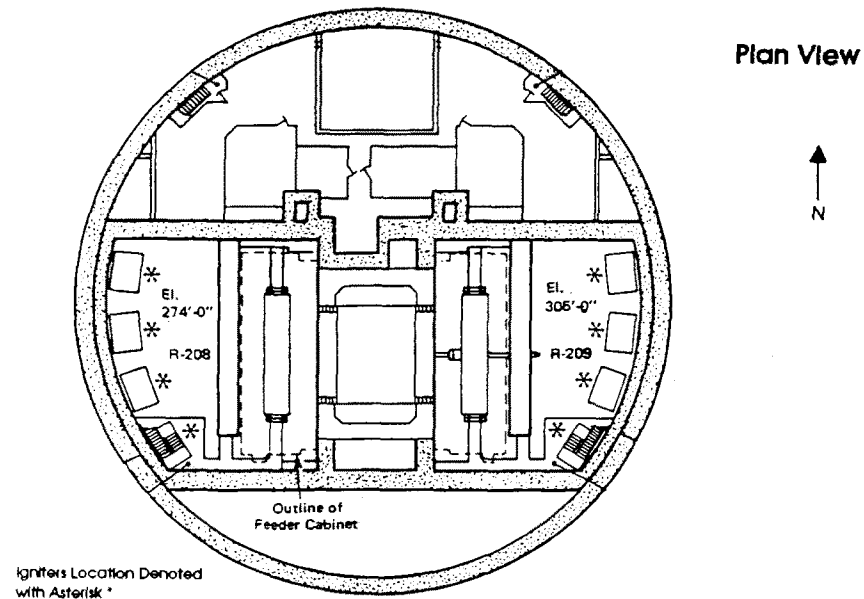
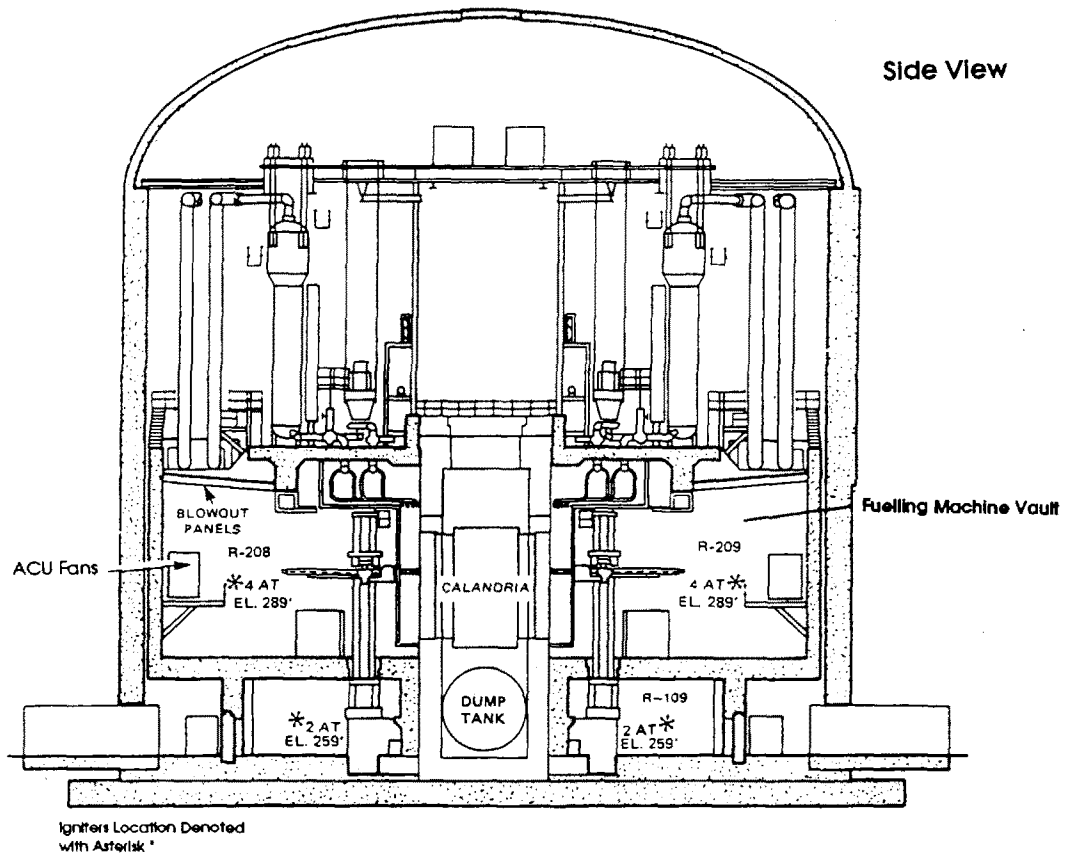


FIGURE 1b Igniter Locations in CANDU Reactor Containment

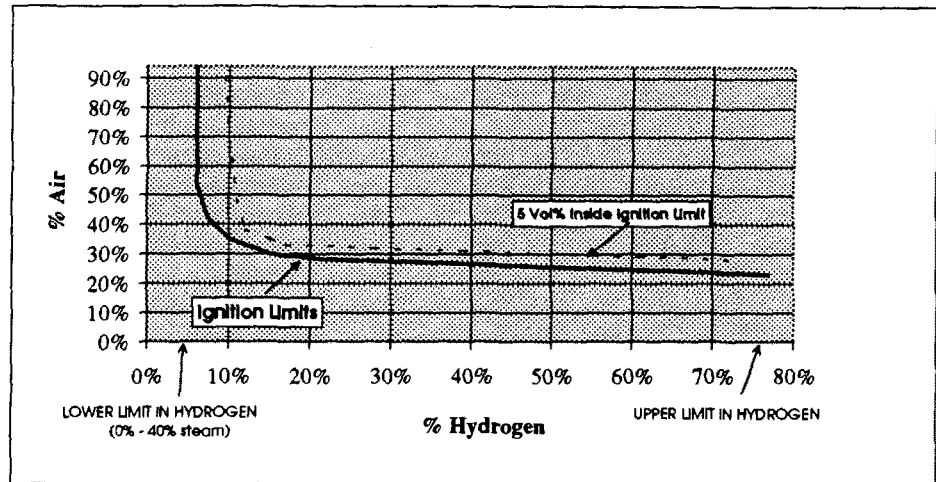


FIGURE 2 Ignition Limits (Glow-Plug Igniters) for H₂-Air-Steam Mixtures

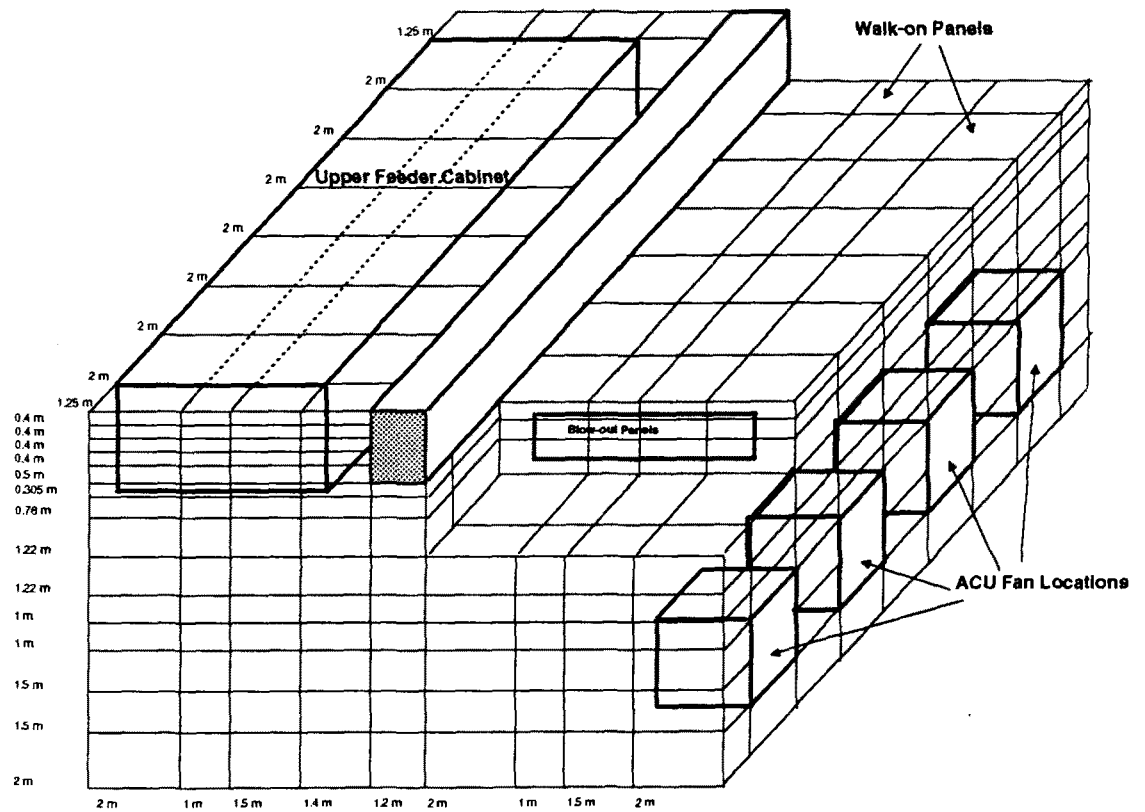


FIGURE 3 Fuelling Machine Vault GOTHIC Model Grid Size

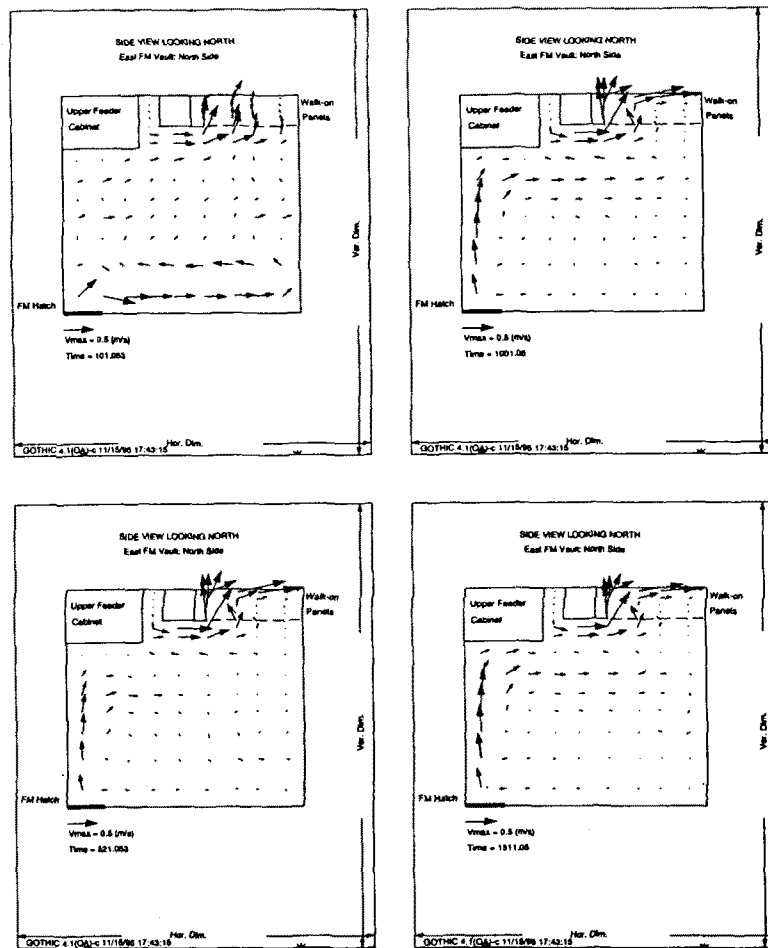


FIGURE 4
Velocity Vector Plots for No Fan Case (north side of FM vault)
at Different Transient Time

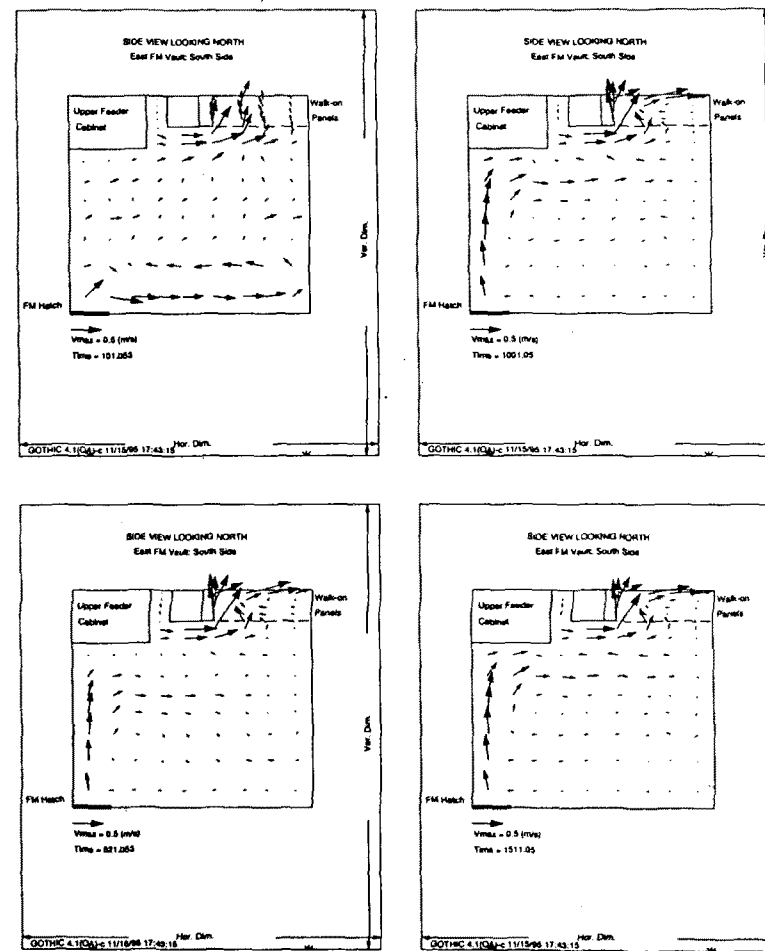


FIGURE 5
Velocity Vector Plots for No Fan Case (south side of FM vault)
at Different Transient Time

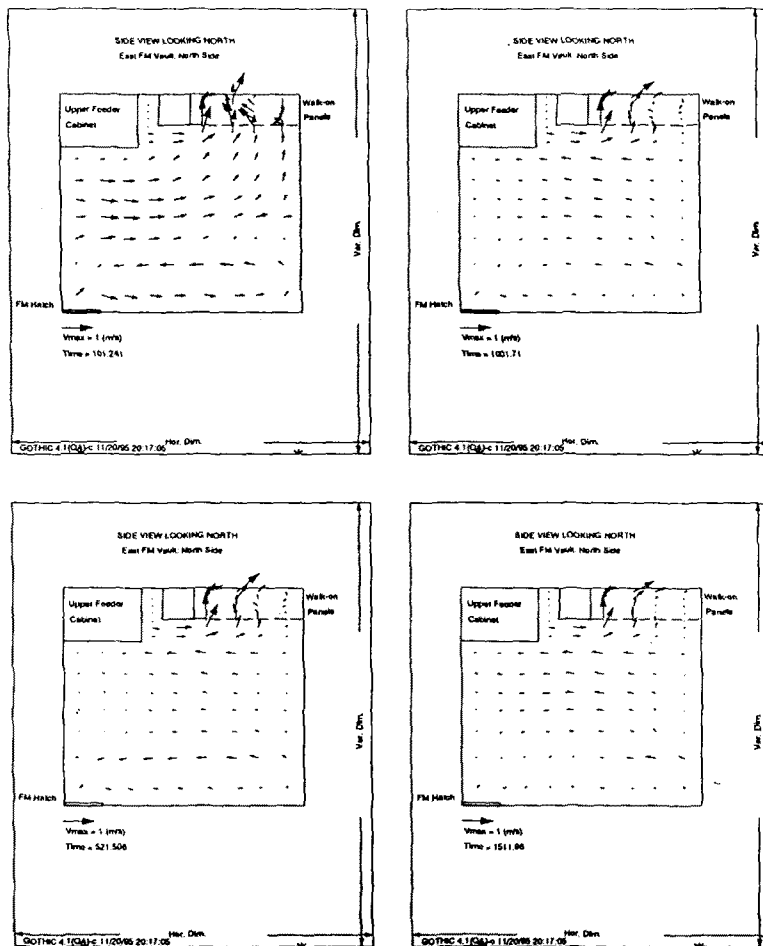


FIGURE 6
Velocity Vector Plots for One Fan Case (north side of FM vault)
at Different Transient Time

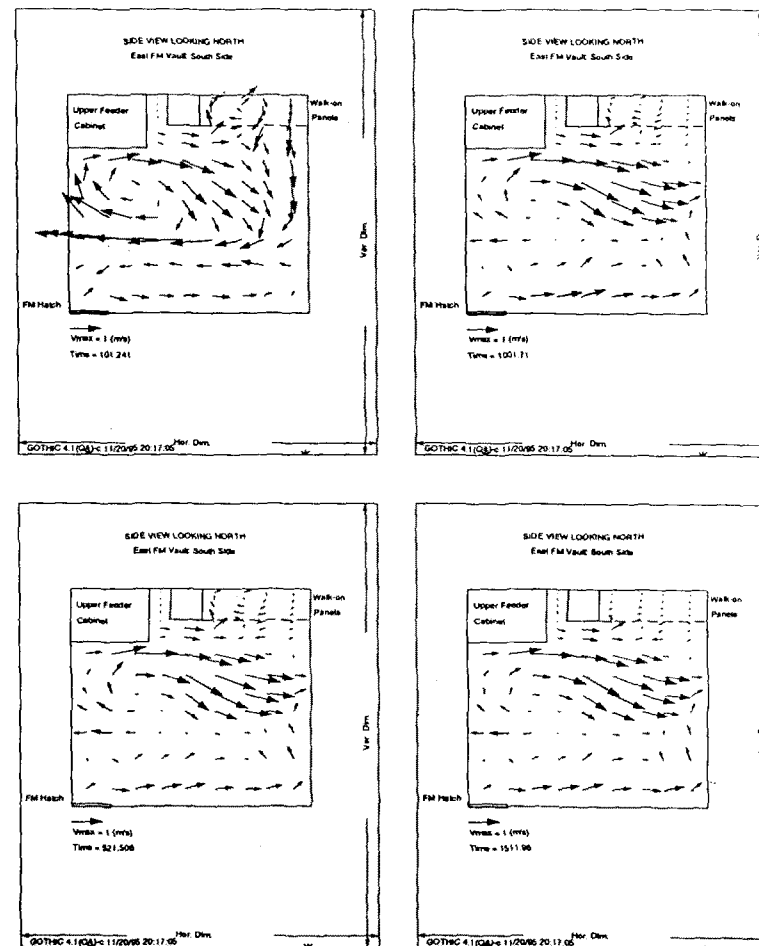


FIGURE 7
Velocity Vector Plots for One Fan Case (south side of FM vault)
at Different Transient Time

FIGURE 8: Spatial Distribution of Hydrogen and Air for 'No Fan Case'
(850 seconds)

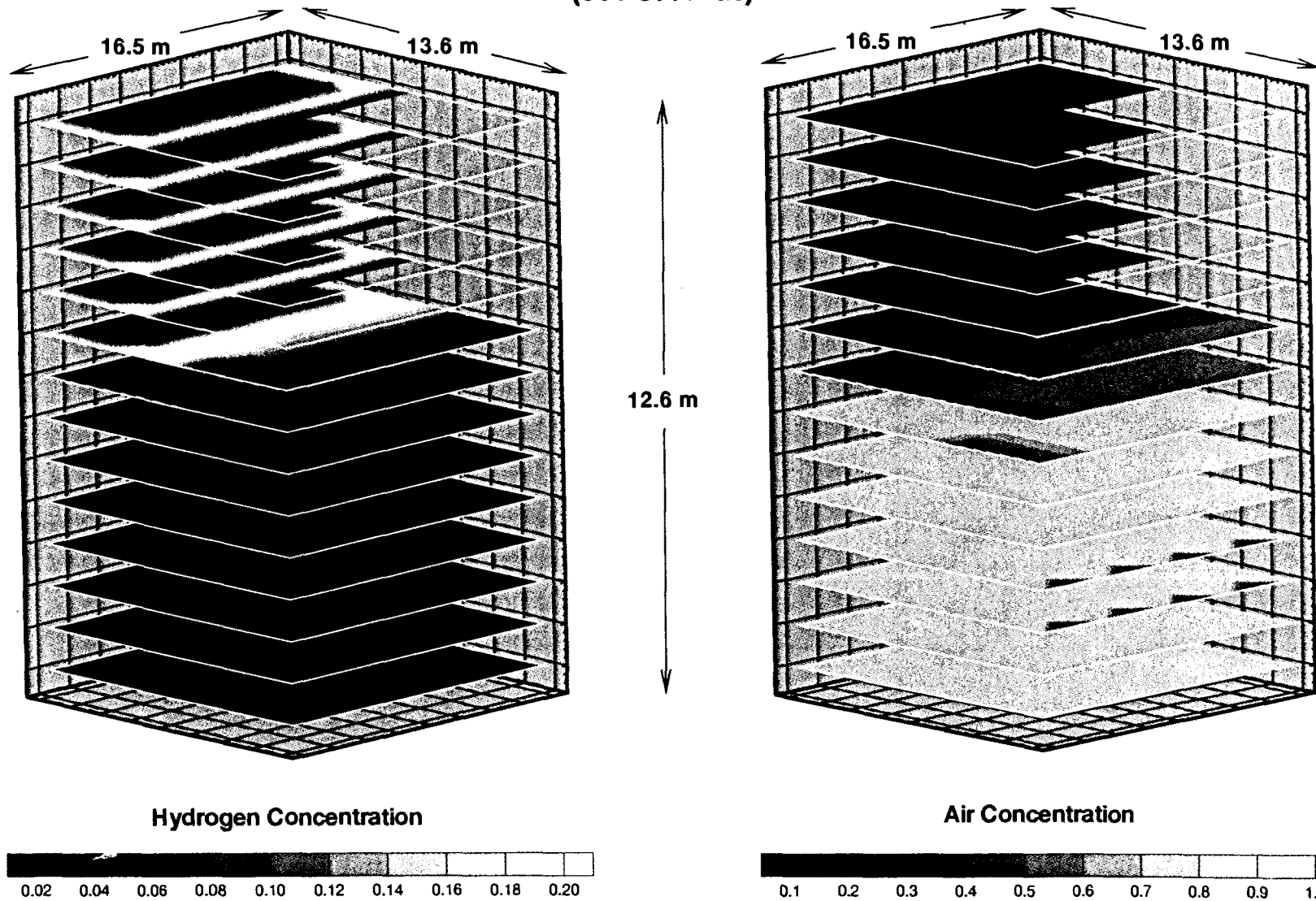
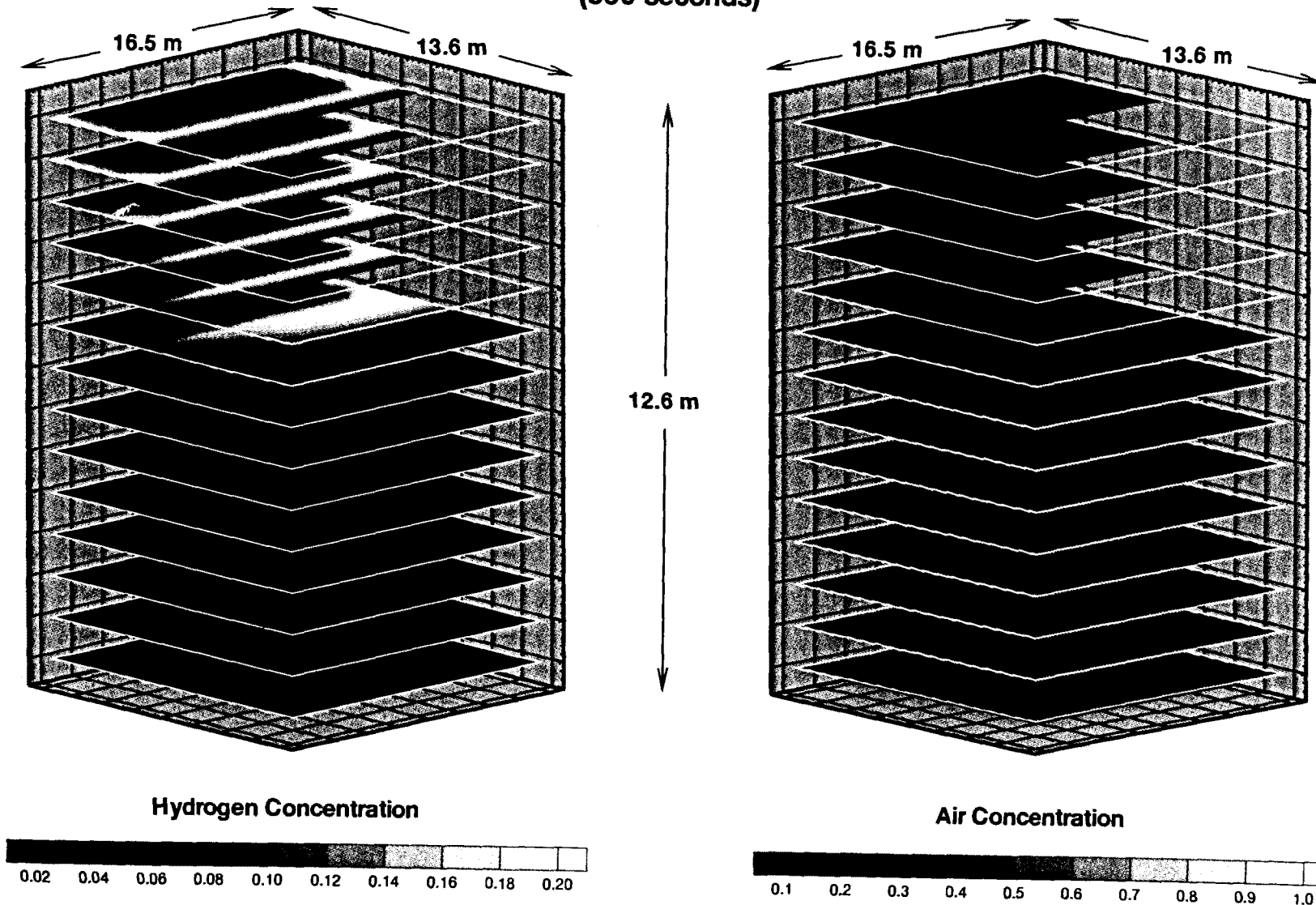


FIGURE 9: Spatial Distribution of Hydrogen and Air for "One Fan Case"
(850 seconds)



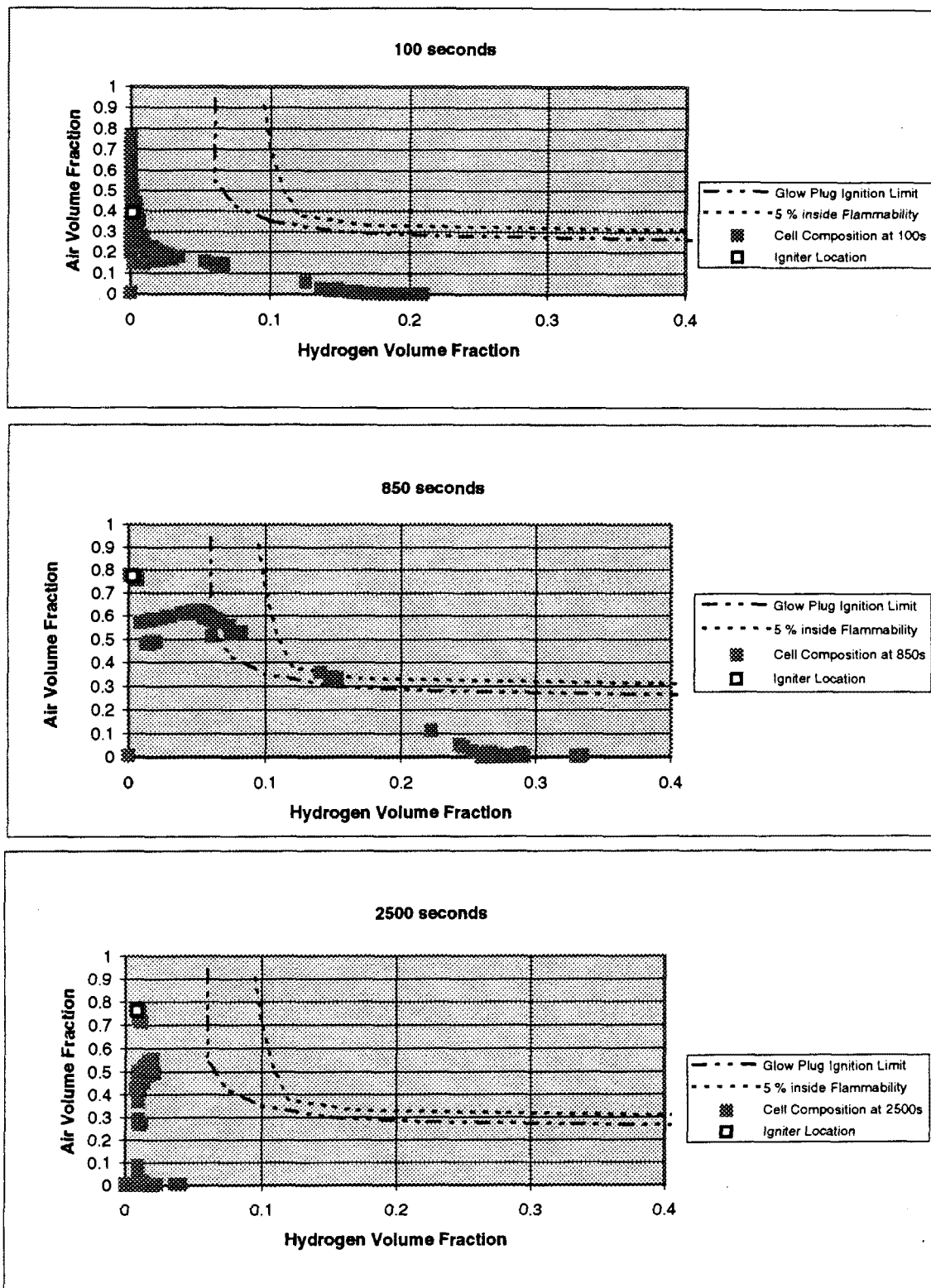


FIGURE 10 Gas Composition in Fuelling Machine Vault Relative to Ignition Limit for No Fan Case

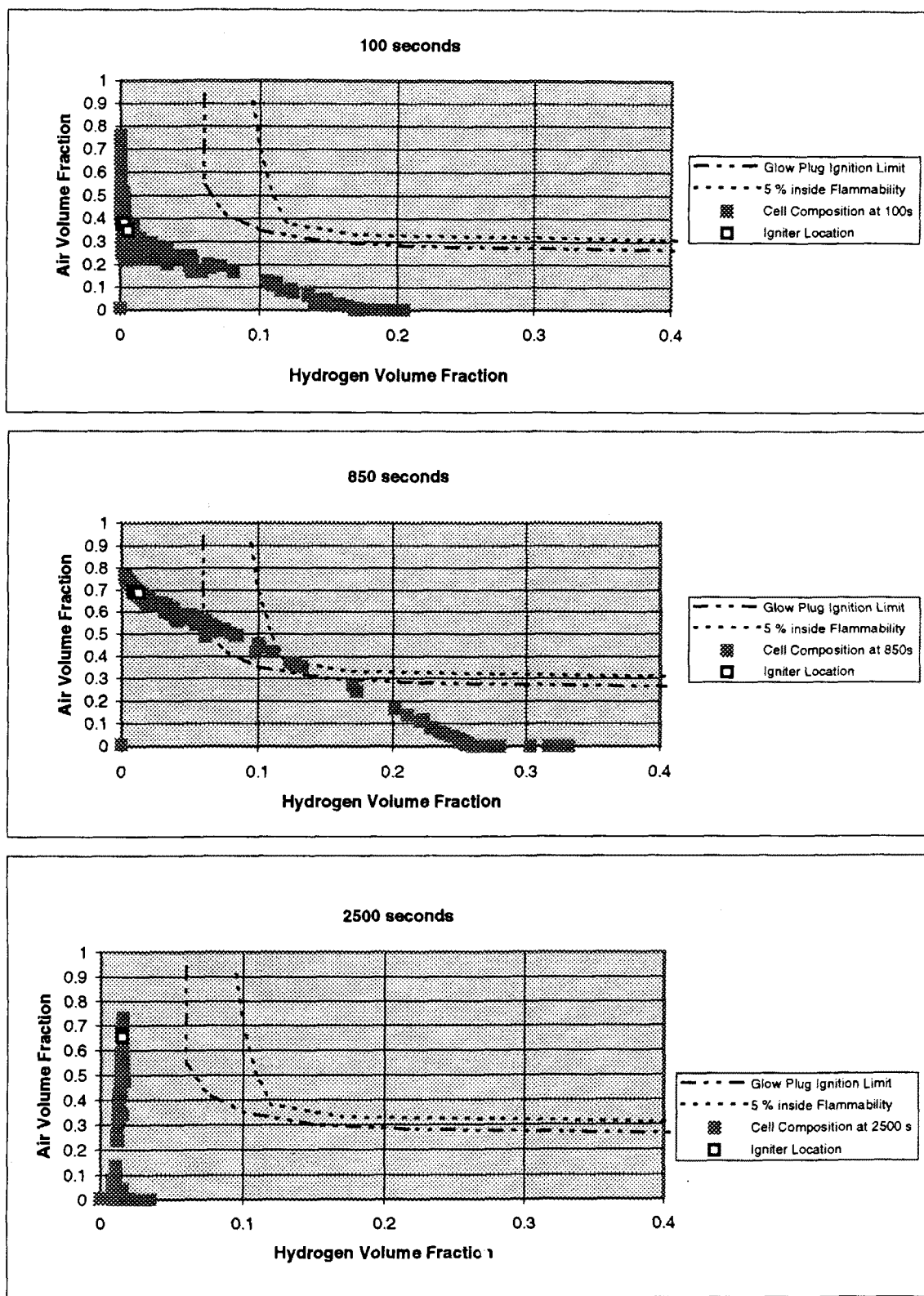


FIGURE 11 Gas Composition in Fuelling Machine Vault Relative to Ignition Limit for 1 Fan Case

2.4 EXPERIMENTAL STUDIES OF HYDROGEN BEHAVIOR IN ICE CONDENSER CONTAINMENTS

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Abstract

A new hydrogen management scheme is being developed for the Loviisa ice condenser containment as a part of a comprehensive severe accident management (SAM) strategy. The scheme relies on containment-wide natural circulations that develop, once the ice condenser doors are forced open as a SAM measure, to produce effective mixing and a slow rise in hydrogen concentrations. Controlled removal of hydrogen from the containment atmosphere, and effective dissipation of the energy in the ice beds are also important elements of the hydrogen management scheme. The VICTORIA experiments in association with scaling and modelling studies are an important basis for the development of the hydrogen management scheme. VICTORIA represents an 1/15th scale model of the Loviisa containment. Numerous experiments have been carried out for studies of the convective flow behavior and hydrogen distribution. This paper focuses on experimental results on the important issues of containment upper compartment mixing behavior and containment behavior in a scenario with depleted ice condensers.

1 Introduction

The Loviisa NPP is a two-unit VVER-440 owned and operated by Imatran Voima Ltd (IVO). The ice condenser containment has a free volume of 58 000 m³, a design pressure of 1.7 bar(a) and a failure pressure of 3.25 bar(a). The lower compartment (LC) with a free volume of 6300 m³ is the release compartment in accidents. The volume of the upper compartment (UC) is 47 000 m³. The flow between the lower and upper compartment has to penetrate the ice condenser (IC). There are two separate ice condenser sections containing a total of at least 835 000 kg ice in normal operation. Long-term residual heat removal is performed by externally spraying the steel shell of the containment dome. The containment geometry is shown in [1]. Glow plugs powered from the emergency diesel generators were installed in the Loviisa containments in 1982, 74 plugs in Loviisa 1 and 66 plugs in Loviisa 2. At the moment, a comprehensive severe accident management (SAM) strat-

egy is being developed for the plant [2]. Current hydrogen mitigation strategies are being reconsidered in this context, see also [1].

The hydrogen management options feasible to the Loviisa containments were discussed in [3]. There it was concluded that the most promising strategy would be based on **providing sufficient mixing of the containment atmosphere, effective energy removal from the containment and controlled removal of hydrogen through passive catalytic recombination (or a combination of passive catalytic recombination and ignition)**. It can be demonstrated that a **global convective loop flow** will take place in the containment during hydrogen release, if the ice condenser doors are forced open as a SAM measure. The loop flow is driven by the developed imbalance of gravity heads of the ice condensers. We can quantify volumetric flow rates and identify the most significant parameters affecting the loop flow rate. These mechanisms and their experimental validation are described in detail in [1].

In addition to being able to estimate the magnitude of the global convective flow, it is essential to understand mixing behavior of the upper compartment. The UC constitutes about 80% of the total free volume of the containment, and therefore its mixing behavior largely determines the overall hydrogen concentration transients being experienced in accidents. A 1/15th-scale model of the Loviisa containment (the VICTORIA facility) has been used for experimental studies of UC mixing behavior, with helium as a hydrogen simulant. A flow visualization system with a two-dimensional laser sheet was used to study the flow patterns of the upper compartment.

This paper focuses on the **results of three new VICTORIA experiments**. Experiment 48 (sections 4.1 and 5.1) was designed to create strong thermal stratification of the upper compartment before helium injection. In experiment 49 (sections 4.2 and 5.2) the upper compartment was almost isothermal when helium injection started. These two experiments, thus, deal with the issue of upper compartment behavior and flow patterns when the initial conditions are varied. Experiment 50 (sections 4.3 and 5.3) studies containment behavior and hydrogen distribution when the ice condensers are empty. The implications of the experimental findings in the reactor scale will be discussed in [1]. For convenience, brief descriptions of VICTORIA, the helium concentration measurement method, and the flow visualization arrangement are included in Chapter 2.

2 VICTORIA test rig and measurement techniques

2.1 VICTORIA

The experimental facility, VICTORIA, was constructed at the IVO Hydraulic Laboratory for studying containment thermal-hydraulics during small-break LOCAs and severe accidents. The facility is a scale model of the Loviisa containment with a linear scaling factor of 1/15, see Fig. 1. The height of the vessel is 4.6 m and the diameter is 3.14 m. The ves-

sel was designed to withstand an overpressure of 2 bar(g). The free volume of the vessel is approximately 21 m³. The model and the Loviisa ice condenser containments are geometrically similar. Concrete structures of the containment are made of concrete in the scale model, too. All the large steel equipment inside the containment, i.e. primary circuit piping, steam generators, pressurizer and pressure vessel are modelled as dummy elements in the lower compartment. The Loviisa ice condenser containment is a double containment. The pressure boundary of the containment is the free standing cylindrical pressure vessel

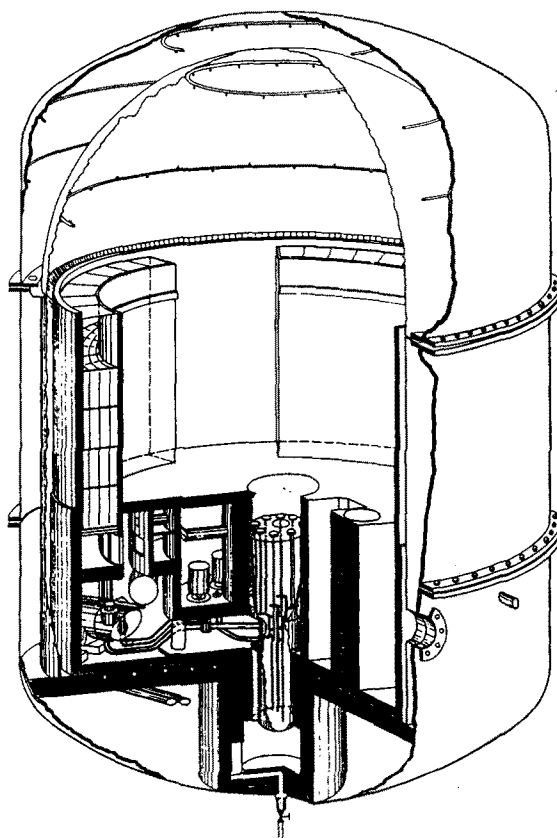


Figure 1 The VICTORIA facility

with a dome inside the secondary containment. In the experimental vessel, the secondary containment is the pressure boundary. The model containment was insulated from all sides in order to minimize heat losses to the environment.

There are six different release locations to select from when simulating the energy and gas release into the lower compartment. Steam, water and helium are released through a common adjustable nozzle directed either downwards or upwards. One of the ice condenser sections at a time can be moved into or out from the model. A section consists of four levels of ice handling baskets with a total of 12 baskets in each section. In one basket there are 15 lattice cases, and in each case there are 6 ice batons. Ice condenser lower in-

let, intermediate deck and top deck doors and bypasses are modelled. The containment internal and external spray systems are also modelled. Temperatures are measured inside the vessel in about 300 points (in air space, structures and ice condensers). Relative humidities are measured at 10 different locations. Total pressure, pressure differences between compartments and water level height are also measured. Further details on the facility can be found in [4], [5].

Helium concentrations in the VICTORIA vessel are measured with a new technique, with an instrument called SPARTA (Spark Transient Analyzer). The small size of the VICTORIA facility motivated us to develop a new concept. Heavy sampling would disturb the flow pattern and change containment conditions in such a small vessel. The sensing element should present negligible disturbance to the flow. The technique should also be fast enough to enable us to follow sudden transients during the experiment. The method being applied in SPARTA is based on emission spectroscopy, where a high-voltage spark is created between two electrodes in a gas mixture. Even a low helium molar fraction shows as growing peaks at certain wavelengths of the spectrum. By measuring the intensity of those peaks, one can derive the helium concentration of the gas mixture. The instrument and its different modes of operation are described in detail in [6].

With an efficient sampling arrangement it was possible to have 10 measurement points in VICTORIA with a single spark chamber. This arrangement was described in detail in [3]. The pumping rate of the 10-channel peristaltic pump could be adjusted. A drawing flow rate of 40-50 ml/min per channel was used. Ten lines were continuously pumped, and the exhaust was close to the sampling point. This way a net flow out from the vessel was avoided. The sampling lines were plastic tubes with a diameter of 2 mm. The sampling and exhaust nozzles were expanded to have diameters of about 5 mm. Thus, sampling gave rise to a flow velocity at the nozzle of only a few centimeters per second, which is low compared to typical gas velocities due to the convective flow in the vessel.

2.2 The flow visualization arrangement

In order to understand the flow patterns and helium mixing behaviors of the upper compartment, a flow visualization system was developed and implemented for the experiments reported here. A laser beam was transmitted through a fiber optic cable into the upper compartment of the facility. The beam was then split into a two-dimensional laser sheet, which efficiently visualizes flow of either a foggy steam-air-helium mixture or smoke used as a tracer. The lower part of the upper compartment usually receives very little steam/fog. Air or an air-helium mixture without steam content is totally transparent (or black) to the laser sheet. Therefore smoke was used to visualize flow patterns of these gases. The flow movements in the upper compartment were recorded with a VCR. The flow velocities could be roughly estimated by adding a mesh to the video picture.

The arrangement in experiment 48 is shown in Fig. 2 a). The figure shows a cross section of the UC. The laser beam splitter was fixed to a point on top of one of the ice condensers. The arrangement in experiments 49 and 50 is shown in Fig. 2 b). Here the beam splitter was mobile, and located in a glass tube between the ice condensers. Four different points

in space could be selected. The beam splitter could also be rotated around its axis to visualize the flow in the dome, at the ice condenser outlets or in the lower part of the upper compartment.

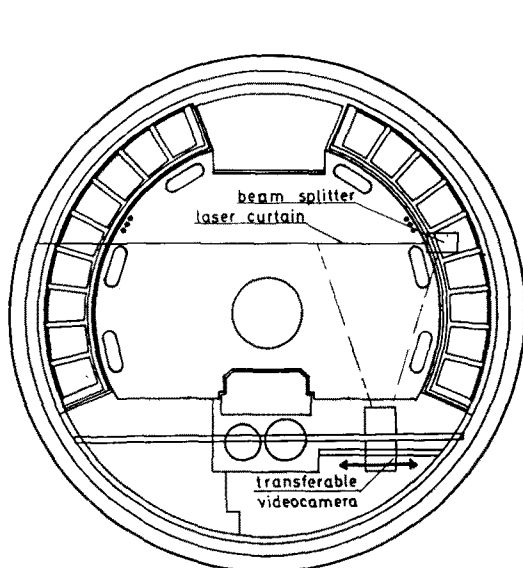


Figure 2 a) Flow visualization in 48

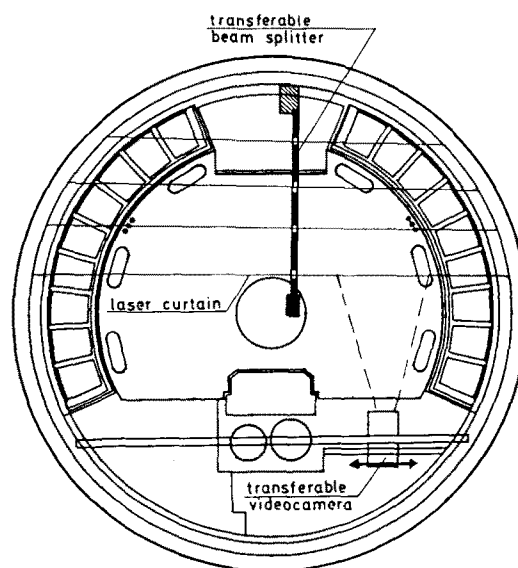


Figure 2 b) Flow visualization in 49, 50

3 Scaling considerations

In order to relate the experimental helium and steam injection histories to severe accident scenarios at Loviisa, we need to understand certain fundamental scaling laws. The velocity field can be scaled analytically from VICTORIA to Loviisa under the assumption that the density differences caused by a helium/hydrogen molar fraction differences between the two ice condenser sections mainly drives the flow. Even though this is a simplification (since temperature effects are omitted), it is realistic in many cases during the hydrogen/helium injection phase (for details, see [1]). The loop flow modelling approach presented in [1] results in the following scaling law for velocities:

$$\frac{u'}{u} \approx \frac{\zeta \cdot \Delta\rho' \cdot H' \cdot \rho_{char}}{\zeta' \cdot \Delta\rho \cdot H \cdot \rho'_{char}}, \quad (1)$$

where u' = the velocity field of VICTORIA (u = the velocity field of Loviisa), H' = the height of the ice condenser in the experiment (H in Loviisa), $\Delta\rho'$ = effective density difference between ice condensers in the experiment ($\Delta\rho$ in Loviisa), and ζ' = the total flow friction parameter in the experiment (ζ in Loviisa), ρ'_{char} = a characteristic density of the flowing gas in VICTORIA (ρ_{char} in Loviisa).

We can obviously conclude that $H'/H = 1/15$ in equation (1). Based on flow friction parameter modelling at flow restrictions of the loop flow path (mainly IC doors and the ice bed, see [1]), we arrive at $\zeta'/\zeta = 0.67$, for similar door opening and ice configurations. Let us assume that the helium injection rate is adjusted to produce the same molar fractions at corresponding times as hydrogen in the containment. In that case the deviation from using helium instead of hydrogen is $\Delta p'/\Delta p = 0.93$. Correspondingly, the scaling deviation of the characteristic densities (assuming a helium/hydrogen molar fraction of e.g. 9%) is $\rho'_{char}/\rho_{char} = 1.007$. Inserting the ratios into equation (1) results in

$$\frac{u'}{u} \approx 0.3. \quad (2)$$

Volumetric loop flow rates will thus scale according to $Q'/Q = (1/15)^2 \cdot 0.3 = 0.0013$. Knowing equation (2), we can study how certain important dimensionless numbers behave:

$$\text{Strouhal number} = Sr \propto \frac{ut}{x}, \quad Sr' = \frac{u' \cdot x' \cdot t'}{u \cdot x \cdot t} = 4.5 \cdot \frac{t'}{t} \quad (3)$$

$$\text{Froude number} = Fr \propto \frac{u^2}{gx}, \quad Fr' = \frac{u'^2 \cdot x'}{u^2 \cdot x} = 1.35 \quad (4)$$

$$\text{Reynolds number} = Re \propto ux, \quad Re' = \frac{u' \cdot x'}{u \cdot x} = 0.02. \quad (5)$$

The Strouhal number ratio equals 1 if the relation between experiment time and real time is $t' = t/4.5$, that is, the experiment time is accelerated with a factor of about 4.5. If we want to inject helium in a way that is synchronized with the convective loop flow, but also produces the correct final, steady-state molar fraction in the vessel, the following law should be followed

$$n' = \frac{1}{15^2} \cdot 0.3 \cdot n \text{ for a duration of } t'_{dur} = \frac{1}{4.5} t_{dur}, \quad (6)$$

where the hydrogen molar release rate n and the release duration t_{dur} should be realistic for accident scenarios at Loviisa.

4 Objectives and procedures of experiments 48-50

4.1 Experiment 48: Stratification of upper compartment

The objective was to confirm the assumptions on upper compartment mixing that were developed based on the findings in previous experiments [3]. There seemed to be two different types of mixing behaviors. A very stable thermal stratification could develop during the experiment in the upper compartment. In this case helium was transferred to the lower

part of the upper compartment mostly by molecular diffusion. On the other hand, mixing seemed to be more efficient when helium entered a thermally fairly uniform upper compartment. In order to confirm the first part of this hypothesis, a **strong thermal stratification was created in the upper compartment before helium injection** in experiment 48. The flow visualization arrangement described in section 2.2 was implemented.

Fig. 3 shows the steam and helium injection history of experiment 48. The helium injection rate (2.85 nl/s during 15.5 minutes, nl/s defined in conditions $T=273K$, $p=1.013$ bar) corresponds to hydrogen from 100% in-core zirconium oxidation released in 4200 s according equation (6). This is a hydrogen release of about 190 g/s during 4200 s in the reactor. The nozzle diameter was approximately 5 mm, and injection was directed downwards. The steam and helium injection point was symmetric with respect to the ice condensers, corresponding to the quench tank in the reactor, see Fig. 4.

The initial ice bed configuration was such that one ice condenser section was filled with ice (all four layers in place), while the other one was empty. The reason for this was our intention to heat up the upper compartment from the beginning. With one empty ice condenser section, steam had immediate access to the upper compartment.

The lower compartment concrete structures were preheated and brought to a temperature of about $55^{\circ}C$ before starting the experiment. The reason for doing this was to better be able to control the scaling distortion from heat transfer to structures in the small-scale test facility by avoiding large temperature differences between the LC atmosphere and LC structures.

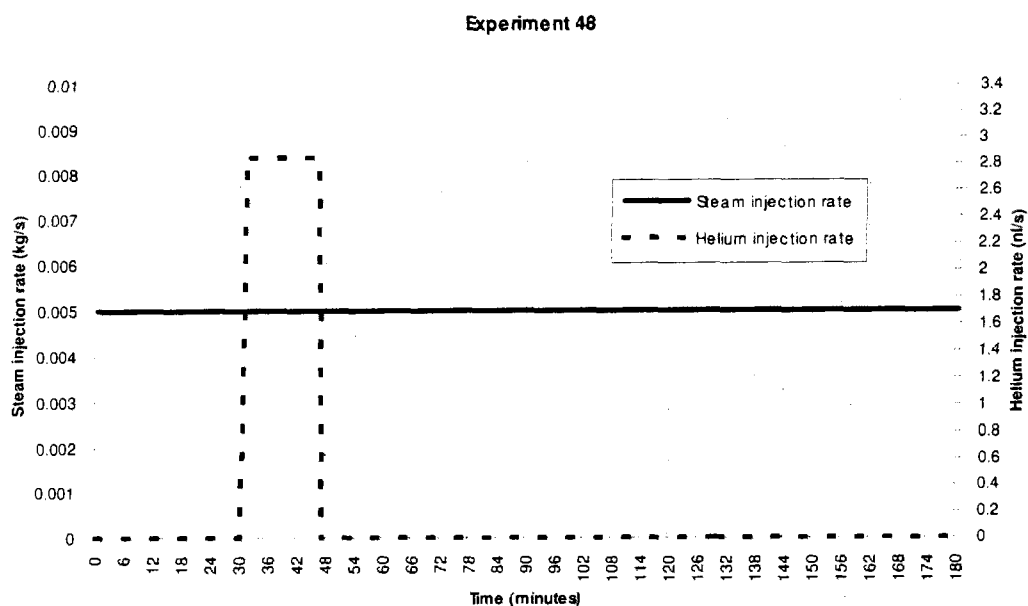


Figure 3 Steam and helium injection rates in experiment 48

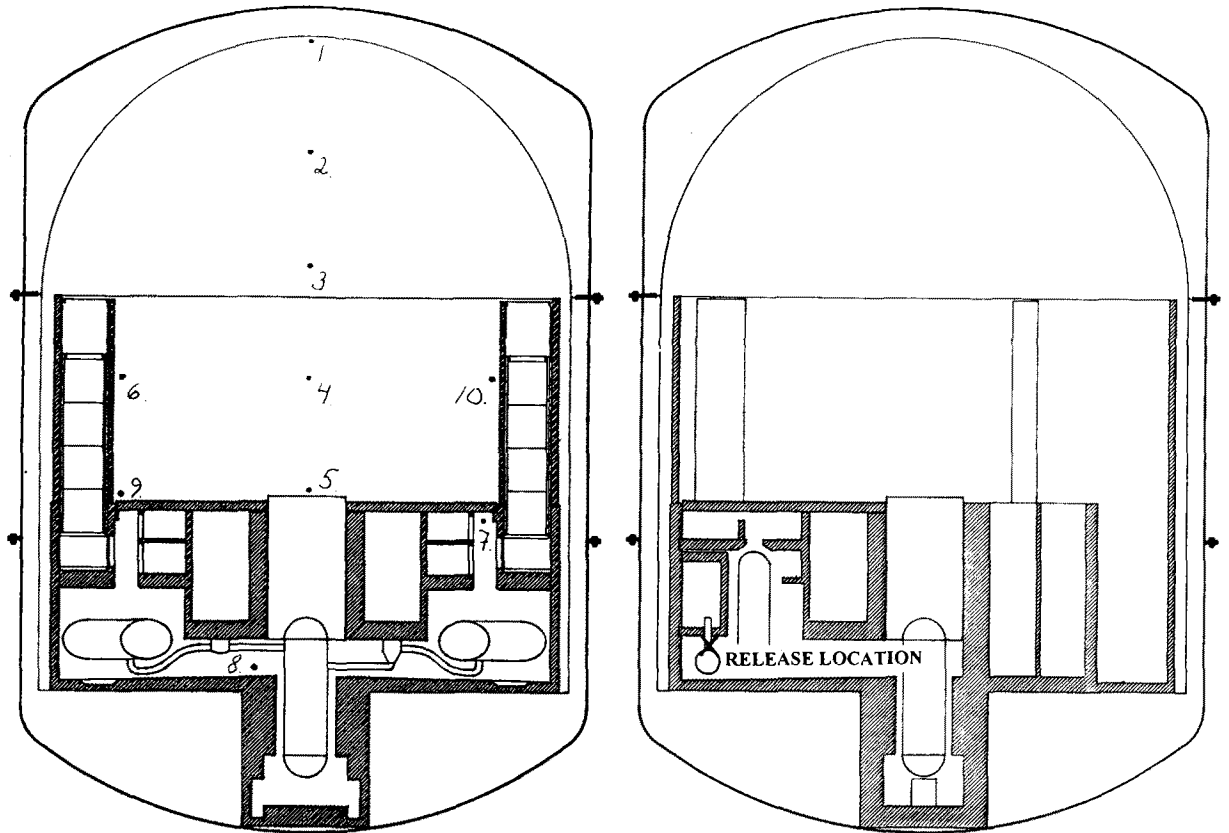


Figure 4 Helium concentration measurement points and release location in experiment 48 (49,50)

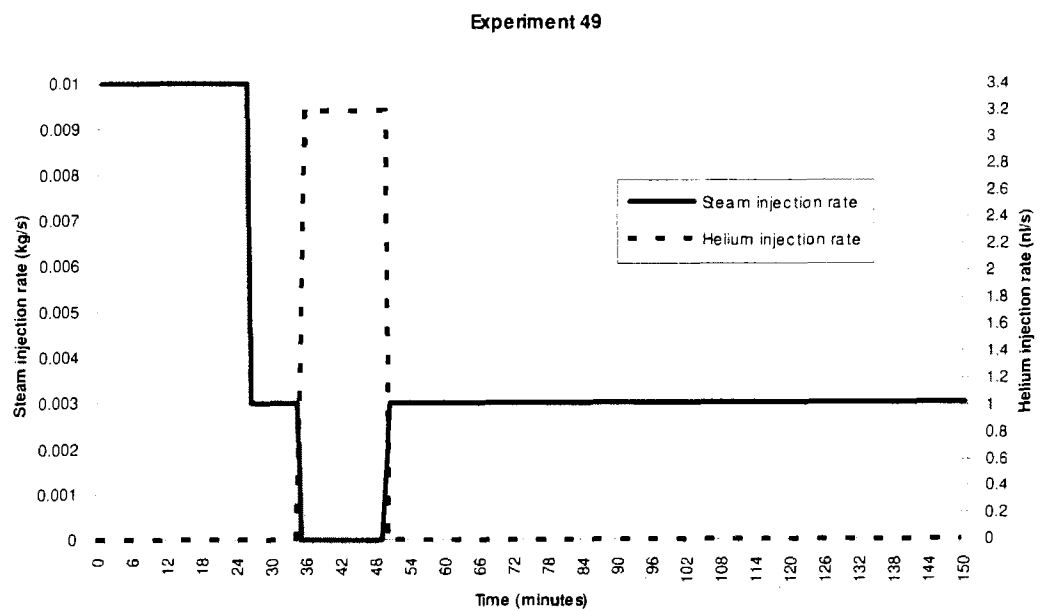


Figure 5 Steam and helium injection rates in experiment 49

At $t=0$, the ice condenser lower inlet doors were forced open with a door opening mechanism. All the intermediate deck door flaps were removed from the facility. The top deck flow area was restricted to 1/3 of the maximum flow area with forced open (removed) top deck doors. The SPARTA sampling points are shown in Fig. 4.

4.2 Experiment 49: Thermally uniform upper compartment

The objective was to create a **thermally uniform upper compartment** into which the helium gas mixture was to enter. The helium injection history should be approximately the same as in experiment 48 in order to facilitate comparison. Flow visualization was an important objective also in this experiment, since we expected a different upper compartment mixing behavior than in experiment 48.

Fig. 5 shows the steam and helium injection history of experiment 49. According to equation (6), the steam injection rate 10 g/s corresponds to 7.5 kg/s and 3 g/s corresponds to 2.25 kg/s in the reactor. The high steam injection rate was maintained until a slight temperature increase in the dome region indicated that steam had penetrated the ice bed. The helium injection rate corresponds to a hydrogen release of about 214 g/s during 4200 s in the reactor (112% in-vessel zirconium oxidation). The release nozzle configuration and helium injection point was the same as in experiment 48.

Due to an experimental error, the helium injection rate was higher than the intended 2.85 nl/s. Afterwards (based on the recorded compressed gas bottle initial and final pressures) we could derive that the helium injection rate was in reality 3.2-3.5 nl/s during 930s. This error, however, in no way rendered the experiment unsuccessful. The main goal, to inject helium mixture into a thermally as uniform as possible upper compartment, was fulfilled. The deviation in helium injection rate only makes it slightly difficult to make direct comparisons to experiments 48 and 50. The different mixing trends, however, can be clearly seen from the experimental data and flow visualization recordings.

The initial ice bed configuration was such that two ice layers out of four were in place in each ice condenser section in a symmetric fashion. The two middle layers were loaded with ice.

The lower compartment concrete structures were preheated and brought to a temperature of about 50°C before starting the experiment.

At $t=0$, the ice condenser lower inlet doors were forced open with a door opening mechanism. The intermediate deck and top deck door configurations were exactly as in experiment 48. Flow visualization of the upper compartment was carried out as described in section 2.2.

SPARTA with 10 sampling points was also used for in experiment 49. The sampling points were the same as in experiment 48.

4.3 Experiment 50: Depleted ice condenser

In principle, the ice beds could be depleted before hydrogen is released into the containment. This would be a very slow severe accident scenario with ample time for different emergency core cooling recovery attempts. The objective of experiment 50 was to melt all the ice before injecting helium. It was of interest to see how efficient the mixing convective flow would be without any ice left in the containment test facility.

Fig. 6 shows the steam and helium injection history of experiment 50. Again, according to equation (6), the steam injection rate 10 g/s corresponds to 7.5 kg/s and 3 g/s corresponds to 2.25 kg/s in the reactor. The high steam injection rate was maintained for 70 minutes. At that point of time, the ice condensers were expected to be depleted. The steam injection rate was thereafter decreased to 3 g/s. At 80 minutes, helium injection at 2.85 nl/s for 15.5 minutes was started. This was the same helium injection rate as in experiment 48. The release nozzle configuration and helium injection point was the same as in experiment 48.

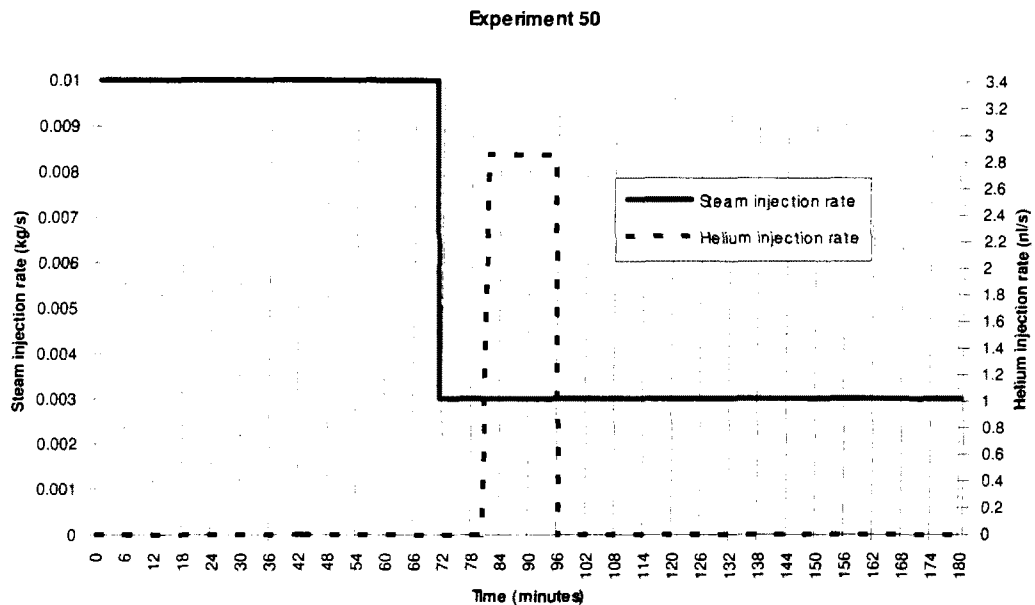


Figure 6 Steam and helium injection rates in experiment 50

The initial ice bed configuration was such that two ice layers out of four were in place in each ice condenser section in a symmetric fashion. The two middle layers were loaded with ice.

The lower compartment concrete structures were again preheated and brought to a temperature of about 50°C before starting the experiment.

At $t=0$, the ice condenser lower inlet doors were forced open with a door opening mechanism. The intermediate deck and top deck door configurations were exactly as in experiment 48. Flow visualization of the upper compartment was carried out as explained in section 2.2.

SPARTA with 10 sampling points was also used for in experiment 50. The sampling points were the same as in experiment 48.

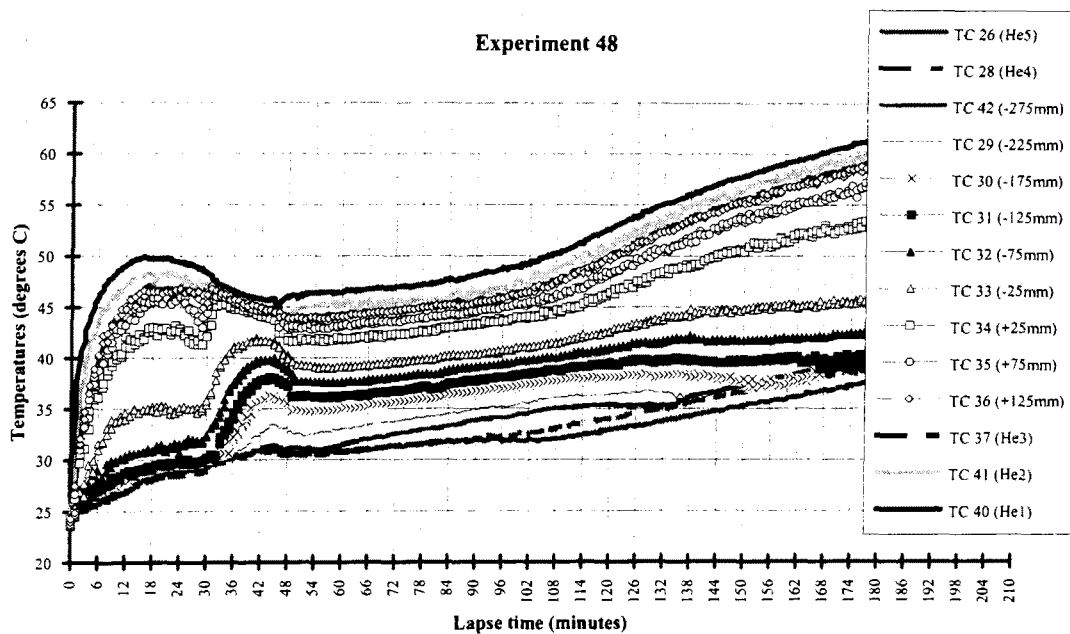


Figure 7 Upper compartment temperatures in experiment 48

5 Results and interpretation

5.1 Experiment 48: Stratification of upper compartment

Fig. 7 shows how the thermal stratification was built up during the initial heat-up phase. The elevations of the temperature measurement points are related to the ice condenser outlet level (0 mm) in the upper compartment. Also the temperatures at helium concentration measurement locations in the upper compartment are shown. Since one ice condenser was empty to start with, steam had immediate access to the upper compartment. The upper compartment stratification situation was fairly stable between 15 minutes and the onset of helium injection at 30 minutes. The density interface extended from level +75 mm (above the ice condenser outlet level) to -75 mm (below the ice condenser outlet level). This

means that the interface had a thickness of 15 cm or slightly less. This result could also be verified by the flow visualization recording in the upper compartment. The stability of containment conditions from about 15 minutes to 30 minutes was also indicated by the pressure history at that time interval, see Fig. 8.

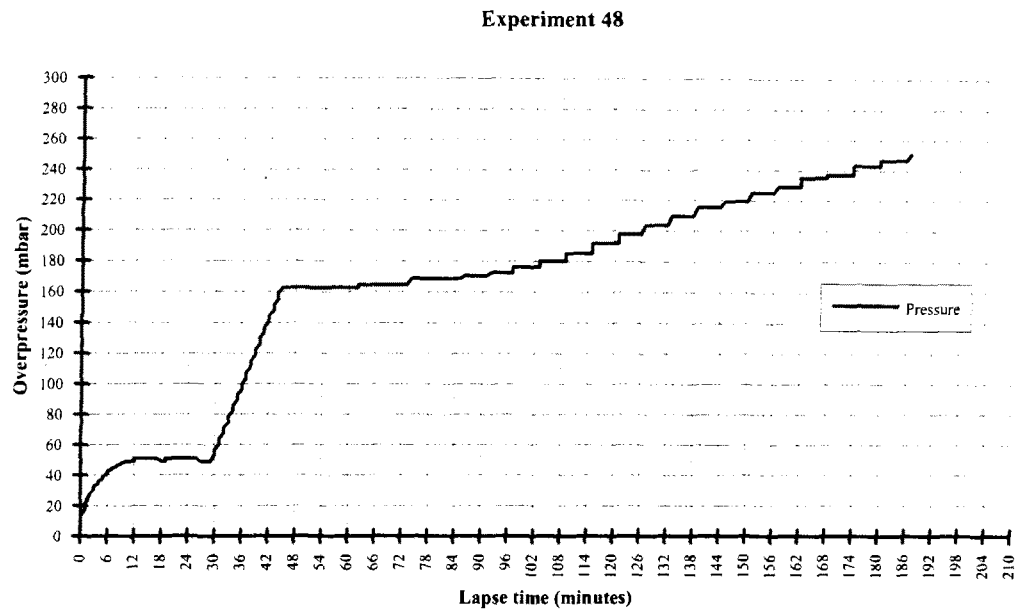


Figure 8 Pressure difference between vessel and surroundings

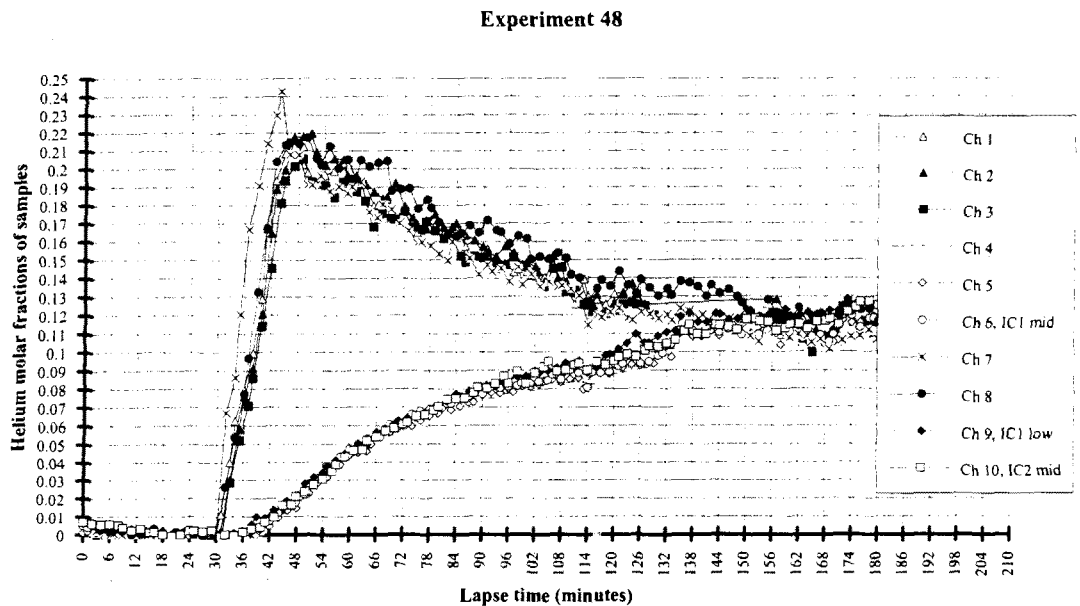


Figure 9 Helium molar fractions in experiment 48

Helium was injected during 15.5 minutes starting at 30 minutes. At the endpoint of the helium injection phase, the points located in the convective loop flow showed helium molar fractions of 20-22% (the lower compartment channel 7 even 24%), see Fig. 9. The points located below the ice condenser outlet level in the upper compartment, however, had helium molar fractions of approximately 1.5%. It took almost 100 minutes (from 45 minutes to 140 minutes) until the upper compartment was completely homogenized in terms of helium concentrations. It was also interesting to note that the space below the density interface at the ice condenser outlet level was well mixed throughout the experiment. Flow visualization showed that there was a convective flow pattern also in the lower part of the upper compartment. It was obviously due to temperature differences caused by slightly heated structures. This flow, however, was not able to penetrate the density interface.

The behavior of the density interface during helium injection can be derived based on the temperature profile measurements of Fig. 7 and on the flow visualization recording. The temperature profile indicates a significant widening of the interface between 30 and 46 minutes, which is the time for helium injection. By the end of the injection period, the density interface extended from level +25 mm (above the ice condenser outlet level) to -275 mm (below the ice condenser outlet level). The interface, therefore, had a thickness of 30 cm, which was twice as much as during the initial steam injection phase. The growing interface can be very clearly observed in the flow visualization recording.

The behavior of the interface can be characterized by the overall Richardson number (Ri), since it is a measure of the stability of stratification:

$$Ri = \frac{g \cdot (\rho_h - \rho_l) \cdot L}{\rho_l \cdot U_{char}^2} \quad (7)$$

where ρ_h = the density of the heavy (lower) layer, ρ_l = the density of the light (upper) layer, L = the thickness of the density interface, U_{char} = the velocity difference across the interface.

The introduction of helium gas into the vessel served to accelerate the convective loop flow pattern. This could be seen from the visualized flow pattern, and was demonstrated by the laser-doppler-anemometer measurements of previous experiments [3]. The accelerated loop flow resulted in an increased characteristic velocity in the overall Richardson number expression (7) along the density interface in the upper compartment. As explained in [7], [8], the turbulent entrainment velocity at an interface increases with a decreasing overall Ri . Entrainment across the interface here served to introduce gas from the lower part of the upper compartment (previously undisturbed by the flow pattern above the interface) into the interface, thus the interface thickness increased. With a growing interface thickness, L in expression (7) increases, and the overall Richardson number increases linearly. At some point, the overall Ri is large enough to render the entrainment velocity and further growth of the interface insignificant.

Later on in the experiment, however, the interface grew thinner due to the flow in the lower part of the upper compartment. This can be seen e.g. at 138 minutes, 150 minutes and 168 minutes in Fig. 7. The reason why the interface could be disturbed from below is the fact that the lower part of the upper compartment was gradually heated in the experiment. This heating was simply due to heating of UC lower part structures - which was, as such, a scale effect. The thick concrete structures in Loviisa would take several days to heat up. The density gradient "maintained" by the dome flow was not large enough to resist the disturbances by the gradually warming (and thus lightening) gas mixture flowing below the interface. The UC was homogeneous in terms of helium molar fractions at this stage of the experiment. The lowest parts of the interface were now entrained into the flow of the **lower** part of the UC.

Based on measurement data on the width of the density interface (and on helium molar fractions above and below the interface), it was possible to estimate the rate of helium transfer across this interface due to molecular diffusion. The helium transfer rate predicted this way very accurately agreed with what was actually measured in the experiment.

Dome region temperature measurements [9] showed that conditions above the density interface were virtually well-mixed. The flow was particularly well visualized when helium injection stopped at 46 minutes, while steam injection continued. One could see from the video recording the flow in the dome region were vigorously turbulent with large-scale recirculation patterns. This caused the dome region to be essentially well mixed. The density interface, however, was an efficient barrier to the flow. We used smoke for visualizing the flow below the interface. From this we could see there was a slowly circulating flow pattern in the lower compartment. Below the density interface there was a slow, wavelike movement.

Temperature measurements in the ice condensers indicated that the ice bed was depleted at 120 minutes [9]. This was followed by increasing temperature trends (Fig. 7) and an increasing pressure (Fig. 8).

5.2 Experiment 49: Thermally uniform upper compartment

In experiment 49, a helium-air mixture entered a virtually isothermal upper compartment. When helium injection started at 34 minutes, there was very little difference between temperatures at high and low elevations of the upper compartment, see Fig. 10. The temperatures at helium channels 1 (on top) and 5 (on main operating deck) are almost identical, and slightly higher than the rest of the upper compartment points. Helium injection took place from 34 minutes to almost 50 minutes. By the end of the helium injection period there was a temperature difference of about 2°C between upper and lower parts of the containment (compared to 15°C in experiment 48).

At the endpoint of the helium injection phase, the points located below the ice condenser outlet level in the upper compartment had helium molar fractions of approximately 6%

Experiment 49

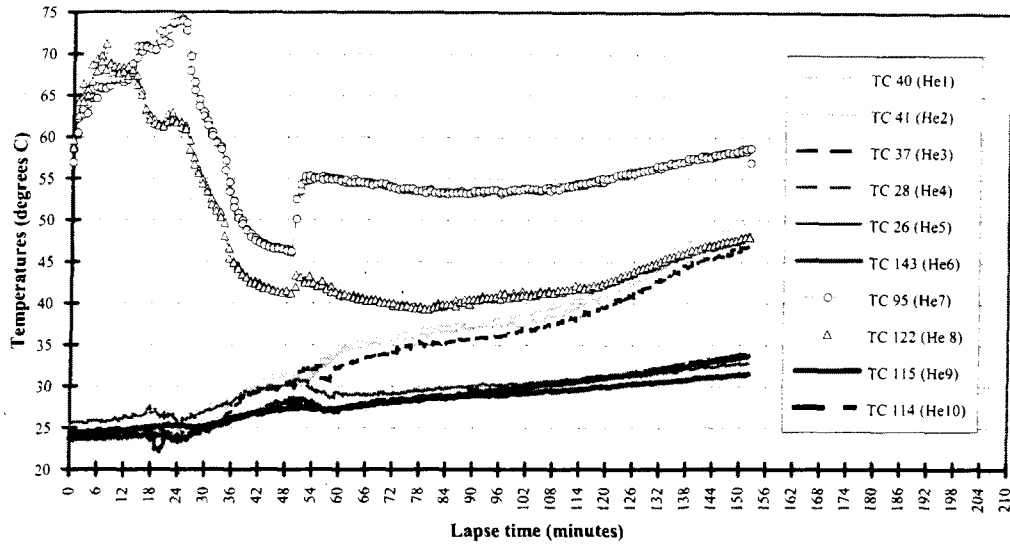


Figure 10 Temperatures in experiment 49

Experiment 49

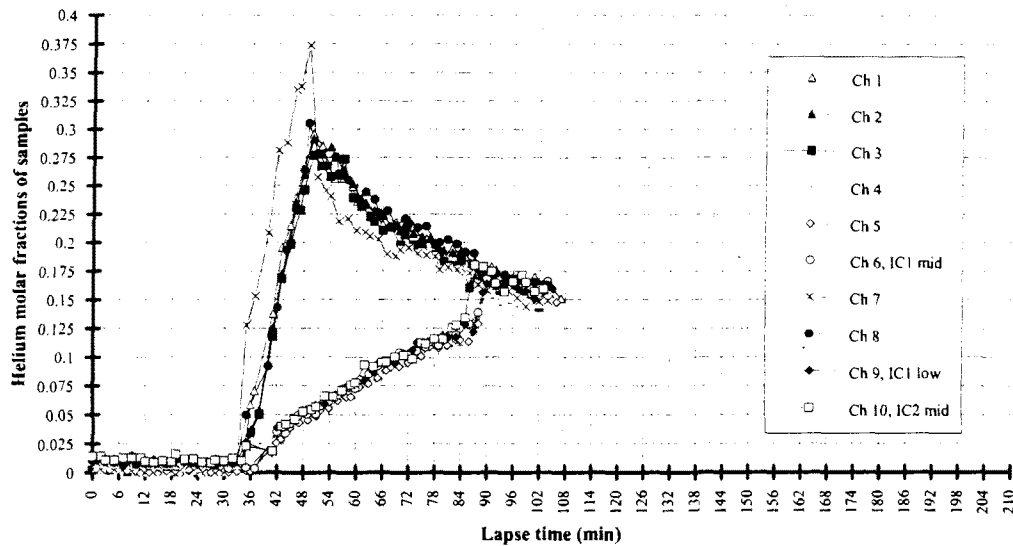


Figure 11 Helium molar fractions in experiment 49

(see Fig. 11), compared to 1.5% in experiment 48. This higher value could be due to different reasons. The helium injection rate was 12-20% higher than in experiment 48. This explains only a fraction of the observed difference. Differences in steam molar fractions

are not a plausible explanation for this part of the upper compartment, either, even though they play a role e.g. in the dome region. This means that helium molar fractions are generally higher in experiment 49 than in experiment 48 or 50, since the steam component has a low molar fraction at low temperatures.

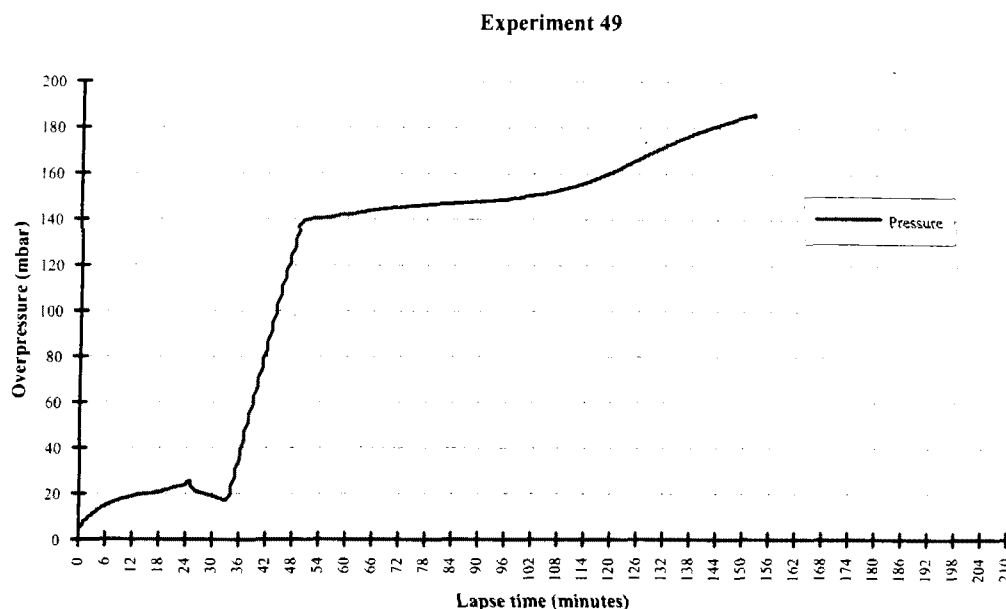


Figure 12 Pressure difference between vessel and surroundings

We have reason to conclude that mixing behavior is completely different from the previous experiment, since the general trend of helium molar fraction growth of the lower part of the UC was different than in the stratified experiment. The equilibrium molar fraction was approached in a nearly linear way. It took only about 40 minutes (from 50 minutes to 90 minutes) until the upper compartment was completely homogenized in terms of helium concentrations. Again, the space below the ice condenser outlet level was well mixed throughout the experiment.

Flow visualization demonstrated that turbulent buoyant plumes entered the upper compartment from the ice condenser. These plumes entrained large amounts of upper compartment gas and created vigorous recirculation flows in the upper compartment dome region also in this experiment.

Flow visualization also showed a different general UC behavior compared to the layered conditions with turbulent dome region, density interface, and slowly circulating lower part in experiment 48. We injected smoke into the lower part of the upper compartment soon after terminating helium injection in order to study the flow pattern in this case. There were distinguishable flows in the **vertical direction** in the region where a density interface had built up in experiment 48. The fairly vigorous flows of the lower part of the UC in this

experiment seemed to prevent the build-up of a thick density interface. The ordered, layered structure was developed later in the experiment due to thermal stratification, but at that point helium concentrations were already nearly equalized.

The introduction of helium gas into the vessel again served to accelerate the convective loop flow pattern, which resulted in an increased characteristic velocity along the ice condenser outlet level in the upper compartment. This resulted in turbulent entrainment of gases from the lower part of the upper compartment into the shear flow. The entrainment strove to create a density interface - now practically only because of helium molar fraction differences - at ice condenser outlet level. However, the flow beneath this interface was vigorous enough, and the density difference small (the Richardson number is directly proportional to $\Delta\rho$), so that the interface was disturbed from below. Since the interface was not "allowed" to grow, the overall Richardson number could not increase sufficiently to prevent gas transport due to turbulent entrainment - in addition to the molecular diffusion processes - across the interface.

Temperature measurements in the ice condensers indicated that they were depleted of ice was at about 120 minutes [9]. This was followed by increasing temperature trends (Fig. 10) and an increasing pressure (Fig. 12).

5.3 Experiment 50: Depleted ice condenser

The objective of experiment 50 was to study a case where the ice condensers were depleted by the time helium injection started. Depletion of the ice beds occurred at 70-80 minutes, judged from temperature measurements in the ice condensers [9]. Therefore there was very little or no ice left in the ice condensers when starting helium injection.

Break-through of the ice bed took place after 20 minutes, as indicated by a slightly decreased temperature (because of cold gas from the ice condenser moving into the upper compartment), followed by a fast temperature increase at helium channels 1-3, see Fig. 13. This initiated a global convective loop flow driven by temperature and steam molar fraction differences in the ice condensers. There was upflow along one ice condenser, a vigorous recirculating flow pattern in the upper compartment dome region (above IC outlet level), and downflow along the other ice condenser. The effect of the onset of the global convective loop flow could also be seen from the accelerated cooldown of the lower compartment (channel 8 in Fig. 13) and the pressure transient in Fig. 14. Upper compartment temperature measurements indicated that a stratification was built up with a density interface at ice condenser outlet level, exactly as in experiment 48.

The helium injection history in this experiment was identical to what we had in experiment 48. Lower helium molar fractions were caused by higher steam fractions, Fig. 15. During the helium injection phase (from 80 minutes to 96 minutes), channels 1-3 and 8 showed virtually identical helium molar fractions and temperatures. This was a result of efficient mixing in the upper compartment dome region and accelerated loop flow.

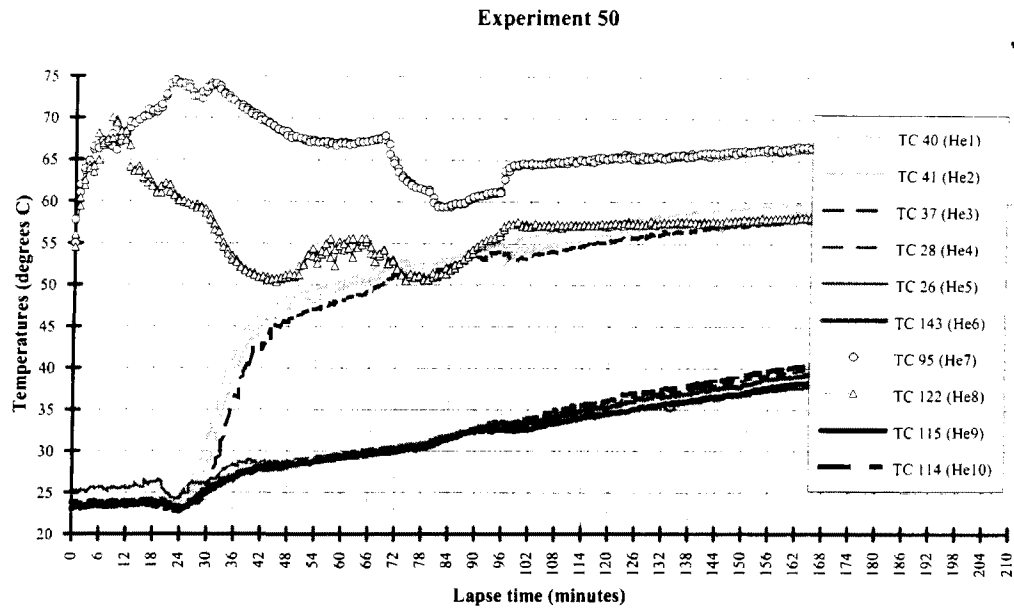


Figure 13 Temperatures in experiment 50

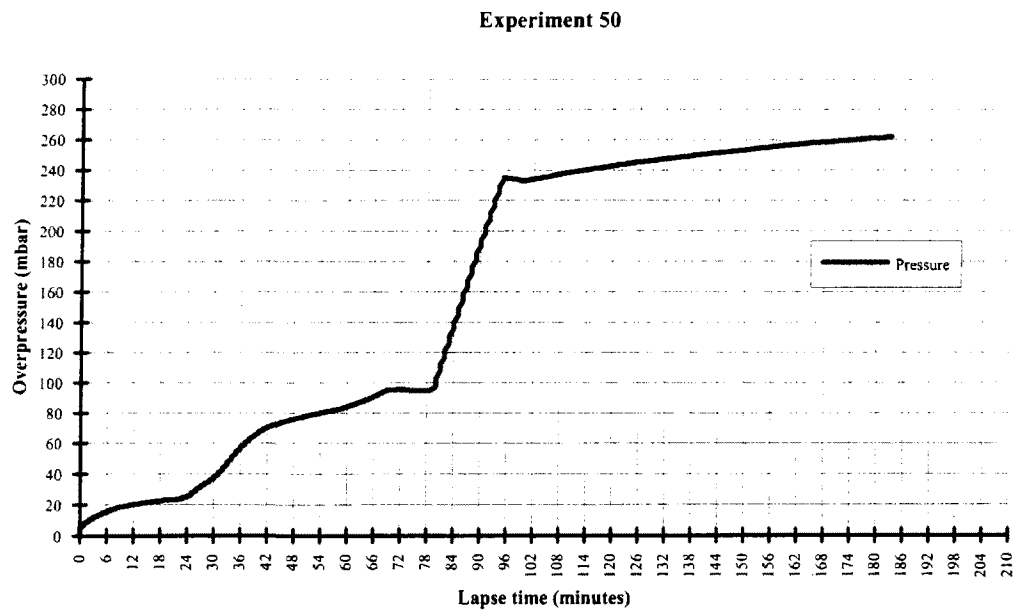


Figure 14 Pressure difference between vessel and surroundings

When helium injection started at 80 minutes, there was a 20°C difference between temperatures above and below the density interface in the upper compartment, which indicated

very stable stratification. Approximately this temperature difference was maintained until the end of the experiment. At the endpoint of the helium injection phase, the points located in the convective loop flow showed helium molar fractions of 15-18%. The points located below the ice condenser outlet level in the upper compartment measured approximately 1.5-2%. It took about 64 minutes (from 96 minutes to 160 minutes) until the upper compartment was completely homogenized in terms of helium concentrations. The helium molar fraction increase trend below the density interface was qualitatively very similar to that of experiment 48, and hence different from the linear behavior of experiment 49. Thus, molecular diffusion could be expected to be the most important helium transfer mechanism in this experiment. The seemingly faster equalization of concentrations in the upper compartment were to a certain extent caused by the higher temperature/steam fraction in the dome region, compared to experiment 48.

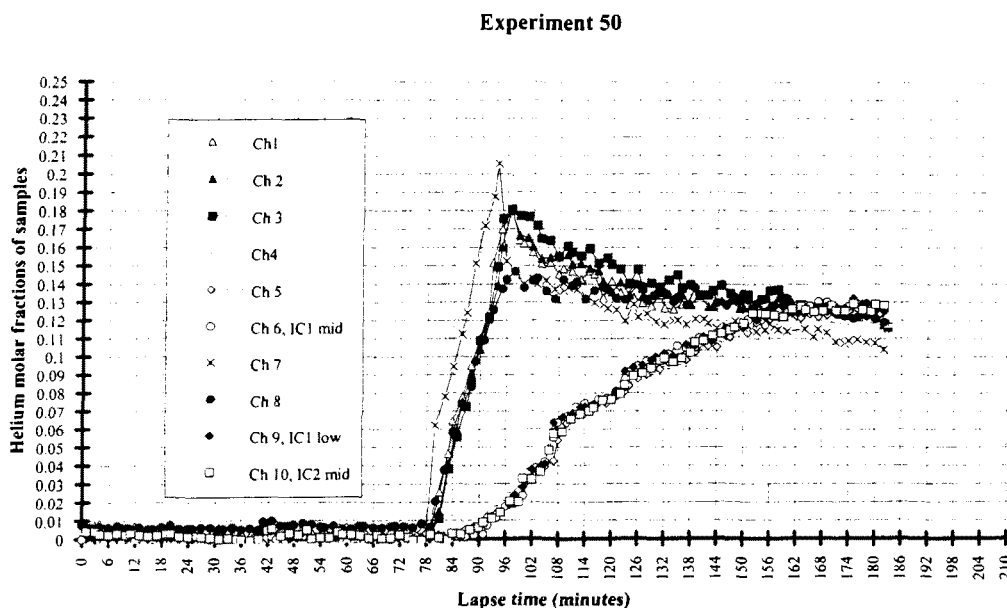


Figure 15 Helium molar fractions in experiment 50

Flow visualization supported the interpretation that the stratification and mixing behavior resembled experiment 48. Again, the density interface was built up during the initial steam injection phase, and grew significantly during helium injection. The reasons and mechanisms were the same as in experiment 48. The thickness of the interface was slightly larger than before. An explanation to this could be a stronger thermal stratification of the upper compartment than in experiment 48. Thus, the flow below the density interface was not to any significant extent able to disturb and thin out the interface.

The absence of ice as such did not affect the mixing behavior. The convective loop flow was driven by thermal and, most importantly, helium concentration differences during the

injection phase. The pressure was rather high in this ice depletion experiment, Fig. 14. The acceleration of the convective flow during helium injection was evident from vigorous mixing in the dome region. The temperature differences in the dome region almost completely disappeared, as indicated by the temperature data in Fig. 13.

6 Conclusions

A new hydrogen management scheme is being developed for the Loviisa ice condenser containment as a part of a comprehensive severe accident management (SAM) strategy. The scheme relies on containment-wide natural circulations that develop, once the ice condenser doors are forced open as a SAM measure, to produce effective mixing and a slow rise in hydrogen concentrations. Controlled removal of hydrogen from the containment atmosphere, and effective dissipation of the energy in the ice beds are also important elements of the hydrogen management scheme. The VICTORIA experiments in association with scaling and modelling studies are an important basis for the development of the hydrogen management scheme. This paper has been focused on experimental results from three hydrogen distribution experiments studying upper compartment mixing behavior and containment behavior in a scenario with depleted ice condensers. The main findings were

- Mixing above the ice condenser outlet level in the upper compartment was seen to be very effective in all three experiments. In particular, helium injection accelerated the global loop flow rate (due to increased imbalance between gravity heads in the ice condensers) and created vigorous recirculation patterns in the upper compartment dome region.
- The dome region recirculation patterns could be seen to be caused by entrainment of gases by the turbulent buoyant plumes entering the upper compartment from the ice condenser.
- The overall mixing behavior of the upper compartment depended on the initial conditions before injecting helium.
- An initially thermally stratified upper compartment developed an ordered, layered structure with a turbulent dome region, a density interface with a thickness of typically 30-40 cm, and slowly circulating lower part at helium injection. Helium transfer across the density interface was slow and caused by molecular diffusion.
- In an initially isothermal upper compartment, a thick density interface isolating the upper region from the lower part of the upper compartment could not be created during helium injection. Thus helium transfer across the interface was much faster than in the initially stratified case.
- The absence of ice as such did not affect the mixing behavior. The convective loop flow was driven by thermal and, most importantly, helium concentration differences during the injection phase. Upper compartment mixing behavior resembled that of the initially stratified case.

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SESSION III

ENGINEERING

Chairman: M. Fischer (Aerospace Research Establishment, Germany)

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3.1 AN APPLICATION OF CONTAIN CODE TO HYDROGEN DISTRIBUTION ANALYSIS FOLLOWING A LOCA IN THE CANDU-6 CONTAINMENT

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ABSTRACT

As a feasibility study on the use of the CONTAIN code for the CANDU PHWR plant, the hydrogen concentration distribution in the short term following LOCA without ECC has been analyzed. The purpose of this study is to verify the hydrogen mixing analysis in a CANDU-6 plant and the adequacy of the location of hydrogen ignitors. The coolant mass and energy discharge rates obtained from the utility's CATHENA calculation results were modified for the CONTAIN analysis with the assumption of flashing at the containment pressure calculated by PRESCON2. The heat removal by the local air coolers were based on their rated cooling capacity. The time-varying flow rates were described to simulate the dousing characteristic. The results show that the calculated peak hydrogen concentrations are about equal to those based on the PRESCON2 model. No hydrogen burn occurs due to low hydrogen concentration. However in case of much enhanced heat removal by the local air coolers, high hydrogen concentrations induce hydrogen burns without significant pressure rise. It is shown that the hydrogen ignitors can play a major role in removing hydrogen. It is necessary to examine further the reason of over-estimation of the accident compartment temperature and the effect of discharge condition and heat sinks on the hydrogen concentrations. Complete modeling of the CANDU-PHWR by the CONTAIN code requires that CANDU-specific material and safety feature models should be available.

1. INTRODUCTION

Three CANDU-6 PHWR plants, Wolsong-2, 3 and 4 unit are being constructed in Korea. These plants will have hydrogen ignitors as a first among the CANDU-6 plants. This design change was motivated by research in the field of hydrogen behavior and the requirements of the Canadian regulatory authority (AECB). The AECB requirements include a refocussed and more stringent analysis of hydrogen behavior and the corresponding mitigating procedures [1]. Stress was placed on the following aspects:

- Distribution pattern of hydrogen in containment and assessment of risks and effects of deflagration.
- Reassessment of functional and reliability requirements of the local air cooler units (LACs).
- Criteria for determining the type, number and location of hydrogen reducing devices (e.g. ignitors, catalytic recombiners).

As the above requirements were also adopted to the Wolsong design, 44 hot surface type ignitors will be installed in the containment - 8 units in each fueling machine (F/M) rooms and 28 units in the steam generator room. These rooms were selected because they are expected to have high hydrogen concentrations which could induce burning following a large break in the primary circuit. The multi-compartment hydrogen distribution analyses were performed by the utility for the 100% reactor outlet header (ROH) break with coincident loss of Emergency Core Cooling (ECC) injection accident which is considered to be the most severe event in

view of hydrogen risk. The analyses were done by using a lumped-parameter code PRESCON2 and also 3-dimensional GOTHIC code for the comparison of each results and the identification of the best locations for the installation of hydrogen ignitors. The results from PRESCON2 calculation show that the peak hydrogen concentration (approximately 7%) occurs in one of two fuelling machine rooms and the steam generator equipment room. The mixture of hydrogen, air and steam can remain flammable from 500 seconds following the accident when sufficient air is available to 1700 seconds when the hydrogen concentration falls below 4% [2]. The GOTHIC code analysis showed that the hydrogen concentration in the accident vault is not uniform during the release period. The highest hydrogen concentration (~15%) was predicted in the vicinity of the broken header in the accident vault. Hydrogen concentrations are predicted about 3% in other areas of the reactor building. It takes less than 2000 seconds to reach a well mixed condition in the reactor building [3]. These results support that the ignitor locations derived by engineering judgment in Wolsong 2,3&4 reactor building are appropriate.

However, it is further required to evaluate the hydrogen ignitors' capability of hydrogen control in the post-accident conditions. There are some other issues regarding this subject such as pressure rise due to hydrogen combustion, flame acceleration, long-term hydrogen behavior, hydrogen distribution in the dead-end compartments, monitoring of hydrogen concentration, and safety equipment survivability under the condition of hydrogen burn.

As a feasibility study on the use of the PWR severe-accident code for the CANDU PHWR plant, an analysis of hydrogen concentration distribution after the above accident has been performed using CONTAIN code, version 1.12 with CRAY Y-MP C90 computer [4]. The purpose of this analysis is to verify the hydrogen mixing analysis and the adequacy of the location of hydrogen ignitors. Hydrogen combustion behavior also has to be examined. Sensitivity study is required to evaluate the effect of engineered safety features such as dousing system and local air coolers. The focus of this study is on the effect of engineered safety feature such as hydrogen ignitors, dousing system, and LACs on the distribution of hydrogen concentration and also on the related thermo-hydraulic parameters such as the containment pressure and temperature.

We had done preliminary analyses for 60% ROH Break with Loss of ECC with a single node [5] and a 39-node model without dousing [6] using the CONTAIN code. The calculated peak hydrogen concentration was well below 4 % in mole fraction for single node calculation. As for the 39-node calculation, the peak hydrogen concentration was 11 % at about 20 minutes after initiation of the accident in the break discharge room. Excessive hydrogen concentration (4-11%) remained for 3 hours following the accident [7]. Following these analyses the accident sequence to be analyzed were expanded to the 100% ROH Break accident in the final safety analysis for Wolsong plants [8]. In response to the changes in the containment model and the break discharge data and etc., we have updated our hydrogen analysis too. This paper includes the analysis

methodology, the results, and some discussion on the problems risen from the analysis.

2. ANALYSIS METHODOLOGY

The release of primary coolant at the design basis LOCA was assumed to have occurred in the fueling machine room (FM107 room) where primary circuit headers are located. The mass and energy discharge data obtained from the utility's CATHENA calculation results contain information on the given time, the mass discharge rate, discharge enthalpy and the specific volume at the just upstream of the break point. Therefore the discharge data had to be modified for the CONTAIN code simulation with consideration of thermodynamic state of the atmosphere following the accident. The release data for each phase of water and steam were provided with the assumption of flashing at the containment pressure which had been obtained from the utility's PRESCON2 analysis. For this calculation a computer program was prepared, including the subroutines for the interpolation of data and subroutines of the 1967 IFC formulation of the thermodynamic properties of ordinary water substance [9]. The released water (D_2O) was assumed to be ordinary water due to limitation of the CONTAIN model which does not have D_2O steam table.

Hydrogen release rate is also obtained from the utility's analysis report which takes into account the metal-water reaction under the condition of the most adverse steam flow rates in the primary system. Hydrogen release continues until 2541 seconds after initiation of the accident. Since this analysis is concentrated on the short-term hydrogen behavior, other hydrogen sources than metal-water reaction were not considered. Although the released hydrogen isotope should be deuterium, it was regarded as hydrogen due to difficulties in the provision of all the required material data for the CONTAIN analysis. It was considered that the difference in the buoyancy effect between these two isotopes could be neglected, compared with other sources of uncertainties.

Spray was modeled to be injected into the steam generator room with all six dousing headers available. The spray flow rate was given as time-varying values with the peak flow rate of 6804 kg/s. The time points of dousing initiation was determined by pre-calculations with no dousing model and the termination time point by pre-calculation with continuous dousing after the first initiation. The initiation pressure was set to 115 kPa(a) and the termination set point was 108 kPa(a). The time to develop full dousing flow and the time to close dousing valve were set to 9.5 seconds and 7.0 seconds respectively. The deceleration rate of dousing flow was set to 3.75 kg/s². Dousing water temperature was set to 28 °C. It was assumed that all 35 LACs including those supplied by Class IV power only were available with their rated cooling capacity. The rated coolant inlet temperature and inlet air/steam temperature were set to 35°C and 49°C. They are not modeled to induce forced circulation but rather to be heat sinks only.

The initial containment temperature and pressure were assumed to 35°C and 1 atmosphere respectively. Additional heat sources such as motors, lights, the reactor faces and piping were conservatively assumed to be zero. No containment leakage was assumed for conservatism.

It was assumed that a burn was initiated and propagated by the hydrogen ignitor according to the criteria of spontaneous ignition and propagation. The threshold mole fractions for the spontaneous ignition are 7%, 5%, and 55% for hydrogen, oxygen, and steam respectively. However, it seems that, in reality, a burn can occur earlier due to the high temperature of hot surface of the ignitor.

The containment geometry data were originally prepared on the basis of the utility's containment model. The model consists of 40

nodes and 80 junctions, as shown in Figure 1. Detailed data related to the link structure and heat structure were given from the design specification of the CANDU-6 plant as per required by the CONTAIN code. The default material property values of the CONTAIN code were used for concrete due to lack of information although the containment structures are consist of two types of concrete, i.e. heavy and ordinary ones. All blowout panels and blowout doors were assumed to be initially intact and leak tight. The maximum design rupture threshold pressures were assumed for them.

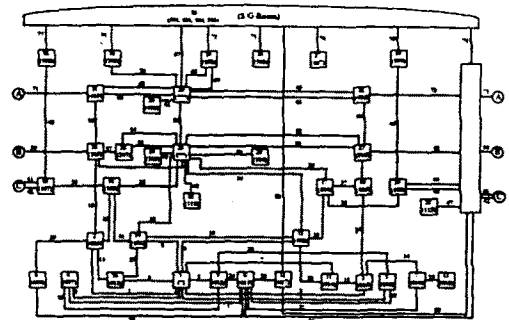


Figure 1 Containment Nodes and Links

3. RESULTS AND DISCUSSIONS

3.1 Results

The 40-node analysis has been done for the hydrogen concentration distribution following the 100% ROH Break.

Figure 2 shows that the calculated peak pressure is approximately 201 kPa(a) at about 7.6 seconds after initiation of the accident. The pressure is reduced quickly by dousing which is actuated from 7.02 seconds following the accident to 166 seconds when the pressure drops to the 'dousing off' set point pressure. After this event the pressure increases slowly and then decreases again.

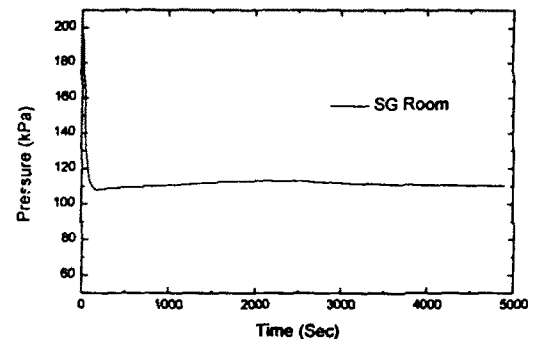


Figure 2 Pressure in the steam generator room

Temperature shows the similar trend to that of pressure for the most subcompartments. However the accident subcompartment has too much high temperature with a peak value of 399 °C at 5.7 seconds following the accident. Figure 3 shows the temperature of

the steam generator room (peak value of 168°C at 7.6s) and the FM107 room.

Figure 4 shows the hydrogen concentrations in the rooms along the major flow path. Hydrogen discharged at the FM107 room moves up to the steam generator room through the steam generator equipment room and then recirculates to the accident room via moderator room. The calculated peak hydrogen concentration of the accident node is 7.0 % at 825 seconds after initiation of the accident. The steam generator equipment room has almost the same concentration as that of the upstream FM107 room. The steam generator room which occupies the largest volume in the reactor building has a trend of concentration increasing until 4% and then reaching to the value of an uniform concentration. Other vaults along the recirculation flow path in the high level show similar trend with the steam generator room. Figure 5 shows hydrogen concentrations in some low regions. It is noted that the hydrogen concentrations do not reach an uniform value until the end of the short term period. There are some dead-end compartments which appear to have very low hydrogen concentrations as shown in the Figure 6.

Hydrogen burn is not occurred for this case due to the relatively low hydrogen concentration.

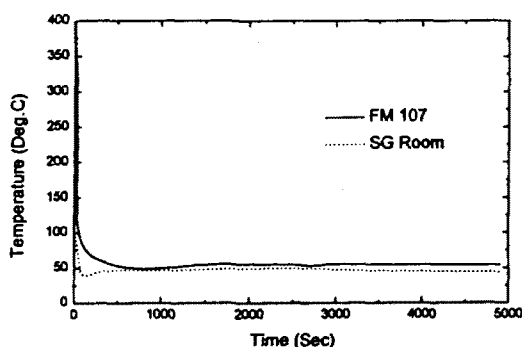


Figure 3 Temperature in the fuelling machine room and steam generator room

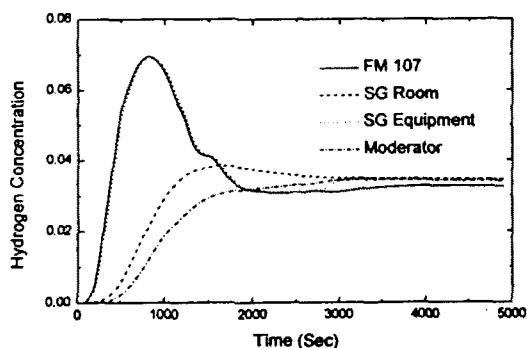


Figure 4. Hydrogen concentrations in the vaults along the major recirculation flow path

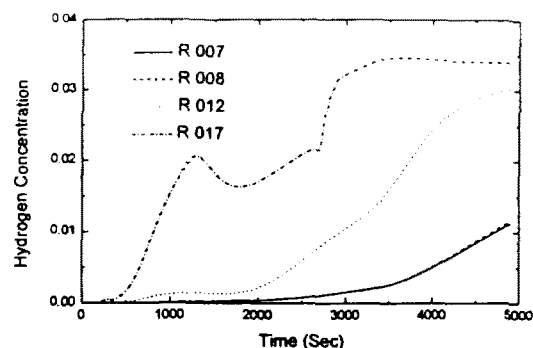


Figure 5. Hydrogen concentrations in the lower vaults

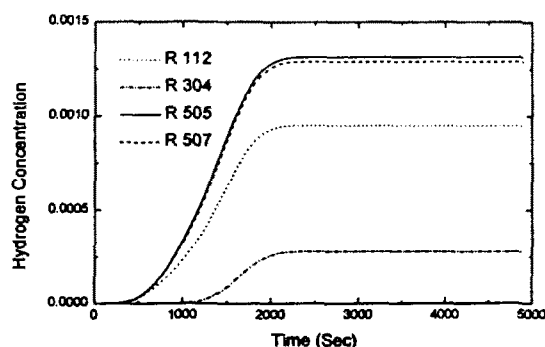


Figure 6. Hydrogen concentrations in the dead-end vaults

3.2 Discussions

The calculated hydrogen concentration agrees well with the reported one of the CANDU-6 plant [2] in the peak value and the trends as shown in the Figure 7. The reported peak hydrogen concentration is 7% which appears at 800 seconds following the accident. However due to the limitation of lumped parameter model the CONTAIN code cannot predict the local peak value, which can be estimated by 3-D code such as GOTHIC.

There are CANDU-6 specific LAC model of enhanced heat removal in the post-accident condition. This cooler model results in far more heat removal than the rated condition. In case this enhanced condition is taken into account in place of the rated condition, the temperature and pressure in the accident node are slightly less than those of the rated condition as shown in the Table 1. However the peak hydrogen concentration becomes 26% due to the induced large heat removal and dousing in the short period. Hydrogen burn occurs several times and reduces hydrogen concentrations in various compartments, especially in the source node which has the peak hydrogen concentration of about 11% as shown in the Figure 8. However there is no significant pressure rise due to hydrogen combustion as shown in the Figure 9.

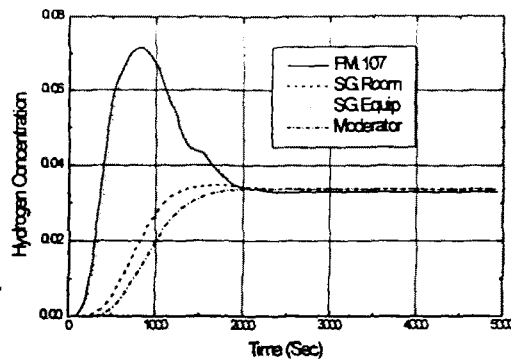


Figure 7. Hydrogen concentrations in the vaults along the major recirculation flow path (CANDU-6 plant)

Table 1 Comparison of main results between the local air cooler models with the rated condition and the enhanced heat removal condition

	Rated heat capacity	Enhanced heat removal
LAC (FM107)		
Cooling capacity	984.4 kW	4451.9 kW
Coolant inlet temp.	35°C	14°C
Air entering temp.	49°C	49°C
Peak temperature	399.4°C at 5.7 s	378.2°C at 5.6 s
Peak pressure	201.1 kPa(a) at 7.6s	192.5 kPa(a) at 7.6s
Peak H ₂ concentration	7.0% at 825 s	26% at 860 s
Dousing start at	0.016 s	0.016 s
stop at	166s	130 s
released quantity	1072.8 m ³	845.3 m ³

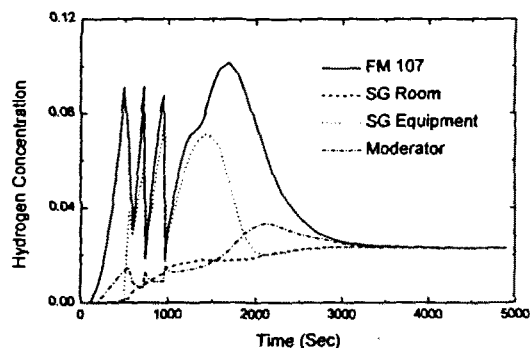


Figure 8. Hydrogen concentrations in the vaults along the major recirculation flow path with hydrogen ignition

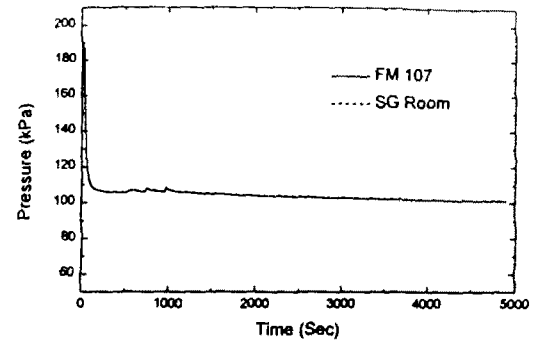


Figure 9 Pressure in case of LACs' enhanced heat removal accompanied by hydrogen burns

Since there is some uncertainty in the treatment of mass and energy release, case studies were done in order to identify the effect of discharge condition on the thermo-hydraulic states of the atmosphere. Two extreme cases of discharge conditions were considered. First, the discharged fluid was assumed as only water vapor with the specific enthalpy of coolant at the just upstream of the discharge point. The analysis results show that the accident node temperature is similar to the values reported by the utility, but the hydrogen concentration is much higher (peak value is more than 20%) than that of the utility's report. Secondly the discharged coolant was assumed as only water liquid with the same specific enthalpy as the other case. This case resulted in unrealistic atmosphere temperature (several thousand degrees in centigrade). Some different options of heat transfer models in the CONTAIN code such as condensation in the wall and forced convection were tried to solve the problem of high temperature in the accident node, but the calculated temperatures were not much different from that of the original case. In order to judge the validity of this study, a further study is required to interpret the high temperature of the accident node.

The low hydrogen concentrations of the dead-end compartments may be under-estimated as noted in the OECD ISP-35 problem [10].

Long term analysis will be required to take into account the hydrogen sources due to radiolysis and corrosion in the containment.

4. SUMMARY AND CONCLUSIONS

The CONTAIN analysis for the short term following a LOCA with LOEC results show that the pattern and the values of hydrogen concentrations distribution agree well with those obtained by the utility using PRESCON2. No hydrogen burn occurs due to relatively low hydrogen concentration. However the accident compartment temperature is predicted to be very high. Therefore, further study is required to interpret this prediction, especially on the discharge condition in the accident subcompartment.

In case the enhanced heat removal by LACs are modeled, high hydrogen concentrations results in several hydrogen burns initiated by the hydrogen ignitors. It is shown that the hydrogen ignitors can play a major role in removing hydrogen in the accident vault. Significant pressure spikes do not appear.

There are limitations in this analysis due to the lumped-parameter model of the code and unavailable material properties. Complete modeling of the CANDU-PHWR by the CONTAIN code needs improvement in the models treating material such as heavy water

and deuterium and the engineered safety feature models to take into account the CANDU-specific design.

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3.2 HYDROGEN COMBUSTION MANAGEMENT DURING A SEVERE ACCIDENT AT THE PLANT WITH THE ICE CONDENSER TYPE CONTAINMENT

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Abstract

In Japan, hydrogen mitigation measures inside the containment vessel during a severe accident are taken against the plant with the ice condenser type containment. Ohi Power Station Unit #1&2, which Kansai Electric Power Co., Inc. owns, are the only plants of this kind in Japan.

Kansai has investigated the hydrogen mitigation measures in collaboration with Mitsubishi Heavy Industry Co., Ltd.

As a result of extensive experiments and analyses, the glow plug type igniter was selected as a hydrogen mitigation device. Environmental conditions were investigated for the purpose of selection of the device. To decide the location of installation, Kansai performed analysis of mixing behavior of hydrogen focusing on the results of small scale combustion testing conducted by Nupec (Nuclear Power Engineering Corporation).

This paper will introduce the detailed results of Kansai's investigation of hydrogen mitigation measures for Ohi Power Station Unit #1&2

I. Introduction

It has been also investigated in Japan if a large amount of hydrogen generated during severe core damage would affect the containment integrity.

The hydrogen management described in this paper is based on the intensive studies initiated in July 1992 when the Ministry of International Trade and Industry (MITI) decided to request accident management (AM) for commercial reactors in response to the policy statement of the Nuclear Safety Commission (NSC) in May 1992.

AM for nuclear power plants in Japan is defined as a voluntary safety enhancement activity and not as a regulatory obligation.

The strategies of AM were presented in detail by the utilities to MITI in the form of "AM report" for each plant at the end of March 1994. MITI judged that utilities' AM strategies were effective for prevention and mitigation of a severe accident through reviewing all the reports, and submitted a report to NSC which included this conclusion on all the Japanese plants at the end of October 1994. NSC also published a report in December 1995 which concluded that utilities' AM strategies were adequate for prevention and mitigation of severe accident.

II. Criteria of flammable gases in Japanese nuclear facilities

The generation of flammable gases in a design basis accident is limited as "the concentration of either oxygen or hydrogen shall not exceed 5% or 4% respectively in the atmosphere inside the reactor containment vessel at least for 30 days after the occurrence of an accident" as described in the NSC's "safety evaluation criteria for light water reactor facilities for power generation"

Although there is no NSC's guidelines clearly defined for the beyond-design basis accident in Japan,

MITI determined that utilities' assumptions were adequate for the hydrogen generation and its consequence as one of severe accident phenomena;

- an assumption of the 75%Zr-water reaction during the core damage was sufficient for the calculation of the amount of generated hydrogen.

- there is little possibility of rapid failure of the containment by detonation if the hydrogen concentration inside the containment was less than 15%.

It is recognized that the hydrogen concentration will not reach the flammable limit even during a severe accident because the atmosphere inside the containment is purged of oxygen with nitrogen in Japanese BWRs.

As a result, hydrogen management is necessary only for Ohi unit-1 and 2 (1175MWe Westinghouse PWR plant), which are accommodated with the ice condenser type containment.

III. Hydrogen Management

The following five subjects are to be considered for the hydrogen management during a severe accident;

1. Select a type of the device which is expected to actuate in an assured manner, considering the containment atmosphere such as temperature, pressure and other chemical conditions during a severe accident.
2. Specify the severest scenario of hydrogen generation to study the effectiveness of the device in mitigating the effects of hydrogen.
3. Understand exactly the mixing behavior of hydrogen in the containment based on the above scenario.
4. Locate the equipment (igniter) inside the containment in consideration of characteristics of mixing behavior of hydrogen and combustion phenomena
5. Verify that containment pressure will not exceed the threshold proof pressure after burning of hydrogen

In this paper, we discuss the hydrogen management for the plants with the ice-condenser type containment from the viewpoints listed above.

III-1. Selection of hydrogen control device

It is necessary to install some kind of the device for the exclusive use of hydrogen control because the existing electric hydrogen recombiner for the design basis accident is hardly able to control a large amount of hydrogen generated rapidly during a severe accident.

The desirable devices include the glow plug type igniter, catalytic igniter, catalytic recombiner and spark type igniter.

The expected functions for the hydrogen mitigation device are as follows;

- ignition : able to ignite in an assured manner when the hydrogen concentration within the installed compartment is 8 % or less.
- mission time : operable 1 week after the occurrence of an accident
- actuation: automatically actuates immediately after the occurrence of an accident
- environmental condition : able to withstand the atmospheric conditions during an accident (spray, steam, iodine, aerosol)

Kansai Electric Power Co., Inc. has carried out experiments on the available devices.

Experimental results are summarized on Table 1. After comprehensive review of these experimental results, we have decided to adopt the glow plug type igniter.

The reasons why we employed the glow plug type igniter are described in detail as follows.

Kansai Electric carried out experiments of three types/four kinds of hydrogen combustion

devices manufactured by three different companies. We examined six parameters to evaluate their performance. The significance of these six parameters and their experimental condition are explained at first and then the results are described.

III-1(1). Lower limit of hydrogen concentration for ignition

It is necessary to confirm that the hydrogen mixture gas is ignited at low hydrogen concentration. In this regard, we carried out experiments on the minimum hydrogen concentration for ignition considering the reliability of ignition and the influence of steam. The criteria is that the hydrogen mixture gas shall be ignited under 8vol% ,which might avoid large scale combustion inside the containment. In addition, the steam concentration ranged below 55vol% which can make the atmosphere inert.

III-1(2) Lower limit of temperature for ignition

In case of LOCA, temperature inside the containment of ice-condenser type plant Ohi unit #1&2 is lower than that of dry type containment plant. Therefore, it is necessary to confirm that hydrogen is ignited at the lower temperature in an assured manner before it accumulates to higher concentration. The experimental condition was set at 283K (10°C, 50° F) at the minimum to envelop the lower limit of temperature inside the containment assumed for accident scenarios of the Safety Analysis Report.

III-1(3) Delay of Ignition

A large amount of hydrogen would be generated relatively in a short period of time in a severe accident scenario. Accordingly, even if the hydrogen concentration reaches to the flammable level in the compartment with a igniter installed, the delayed ignition may cause a large scale of combustion followed by the failure of the containment when the concentration exceeds 8vol% in a wide area with delay of ignition.

Consequently, we measured the time delay of ignition through experiments for each device.

Allowable time delay for ignition was assumed to range from 4 through 36 seconds considering the rate of hydrogen generation and the free volume of compartment.

III-1(4) Resistance against poison

It is also necessary to take into account the fact that a lot of iodine and aerosol inside the containment may adhere to the surface of igniter in a severe accident. For the catalytic igniter, it is necessary to check the possibility if the iodine degrades the activity of catalyst and if the aerosol attached to the surface of catalyst prevents contact with hydrogen gas.

And it is necessary to check if degradation of insulation caused by aerosol stuck to the surface prevents spark generation because the spark type igniter generates spark by the potential difference between electrodes.

III-1(5) Gas velocity

The gas velocity around the hydrogen combustion device has an influence on the ignition.

For the catalytic igniter, the lower gas velocity generally makes the amount of hydrogen supply less, which may lead to inflammability. Accordingly, the gas velocity varied from 0.3 to 5 m/s in the experiments and successful ignition was required in this range.

III-1(6) Countermeasures against spray water

There are two trains of containment spray system inside the containment of Ohi unit #1&2. It is necessary to verify that the spray water onto the hydrogen combustion device may not cause degradation of its performance.

III-1(7) Experimental results and evaluation

The candidate devices were investigated through experiments and studies regard above six items.

As a result, it has been proved that the igniters are designed so that spray water during an accident may not cause any trouble and all the devices meet the requirement on the gas velocity.

As summarized in Table 1, the catalytic igniter showed low reliability in four items; lower

limit of hydrogen concentration, ambient temperature for ignition, delay of ignition, and resistance against poison. Meanwhile, the spark type igniter did not meet three criteria except for the lower limit of hydrogen concentration for ignition.

The reason why the spark type igniter was determined not to be accepted because of lower limit of temperature for ignition and delay of ignition depends on the fact that the temperature switch doesn't properly operate under the atmosphere at low temperature rather than the performance of the igniter itself.

On the other hand, it was confirmed that the glow plug type igniter met all the criteria.

In conclusion, we have decided to employ the glow plug type igniter as a hydrogen mitigation device in a severe accident for Ohi unit #1&2.

III-2. Selection of analysis scenario

We have investigated to determine the severest scenario with a large amount of hydrogen generated.

For the analysis with use of MAAP3.0, the following scenarios were considered:

S2D (small break LOCA + failure in ECCS injection)

S2H (small break LOCA + failure in ECCS recirculation)

TMLB' (loss of main feed water + failure in auxiliary feed water system)

S1H (intermediate break LOCA + failure in ECCS recirculation)

As shown in Table 2, the analytical result indicated that the maximum amount of hydrogen generated was equivalent to about 45% Zr-water reaction amount and the maximum hydrogen generation rate was equivalent to 1.1 kg/sec. Considering these analytical results and scenarios, it is indicated that the deeper the core is exposed and the larger the amount of water vapor supplied to the core, the larger the amount and generation rate of hydrogen are.

Table 2:Hydrogen Generation in a Severe Accident

Scenario	Amount of hydrogen generated(*)	Maximaum hydrogen generation rate	Initiation of core melt
S2D	41%	~ 1.1kg/sec	1.3hr
S2H	39%	~ 1.0kg/sec	7.4hr
TMLB'	40%	~ 0.5kg/sec	1.6hr
S1H	30%	~ 0.5kg/sec	2.0hr

(*)Zr-water reaction equivalence

The large amount of hydrogen generated results in a severe effect in view of the increase in the containment internal pressure when hydrogen is burnt, while the high generation rate results in a severe effect in view of the efficiency in hydrogen control

In addition, when considering the initiation of hydrogen generation, 1 week of the mission time is sufficient for the hydrogen burning device since the occurrence of the core damage would be less than eight hours in all sequences shown in Table 2.

Based on the above considerations, the scenario S2D (small break LOCA + failure in ECCS injection) was selected as the severest one, and investigation was conducted as follows

assuming the amount of hydrogen generated as equivalent to 75% Zr-water reaction amount.

III-3. Investigation of hydrogen mixing behavior inside containment vessel

Next, we analyzed hydrogen mixing behavior inside the containment vessel. The scenario to be analyzed was S2D with the assumed amount of generated hydrogen equivalent to 75% Zr-water reaction amount as mentioned in the previous section. The Fig.1 shows the time transient of the release rate of hydrogen generated.

We employed MAPHY-BURN code for the analysis. As shown in the Fig.2, the node-path model was established with the containment vessel divided into 23 compartments.

As shown in the Fig.3, the analytical result indicated that the concentration of hydrogen at the bottom of the ice condenser inlet followed, with a constant time lag, the change of the hydrogen concentration within the compartment where hydrogen was generated. The peaks at the hydrogen concentration correspond to the increases of the amount of generated hydrogen.

This analytical result showed that hydrogen generated at the lower compartment went up to the upper part of the containment vessel through the ice condenser. This flow path of hydrogen is what expected in the design of the ice condenser type containment vessel.

III-4. Selection of location where igniter to be installed

Considering the hydrogen flow path shown in the analytical result as described in the above section, we have selected the location where igniters were to be installed.

Large scale hydrogen combustion in a broad space may challenge the containment vessel integrity. Accordingly, it is desirable to burn hydrogen locally at lower concentration. Therefore, it is designed that igniters should be located on the hydrogen mixing path and supplied power on actuation of the safety injection signal so that they can get igniting condition prior to hydrogen generation. It has been confirmed that sufficient power capacity is assured though it is supplied from the emergency power source.

The Fig.4 shows the locations of igniters.

On the other hand, the result of the small scale hydrogen combustion test conducted by Nupec under the contract with MITI showed that the way in which flame propagates depends on the hydrogen concentration[1]. As shown in the experimental result of the Fig.5, the flame propagates only to the limited area with the hydrogen concentration at 4 % while the flame gradually propagates upward at 6 % and then rapidly spreads over upward and downward at 8 %. As can be seen from these results, it is important to burn hydrogen at its low concentration for mitigation of the effect on the containment vessel integrity.

We are also investigating the locations of igniters to be installed at the plant site, and details will be soon revealed.

It is planned to install a simple cover on the glow plug for protection against spray water injected during an accident and for protection during the annual inspection.

III-5. Analysis of Containment vessel internal pressure

Finally, it is necessary to verify that the containment vessel internal pressure will not exceed the limit when the installed igniter actuates. The design pressure of the containment vessel at Ohi unit 1 and 2 is equivalent to 0.84 kg/cm²G, while it is expected that the containment vessel can withstand up to about 3 times the design pressure.

Based on the above scenarios, an analysis was conducted for the severest condition in view of the containment pressure increase, assuming that hydrogen begins to burn at the concentration of 8 % within the compartment where the igniter is installed and then hydrogen thoroughly burn out. The result showed that the containment pressure would not exceed the containment design pressure and that the containment integrity was maintained (Fig.6).

It was assumed to install 17 igniters in this analysis. Actually, the number of igniters to be installed will be doubled to 34 to allow for some margin.

IV. Considerations on installation of igniter

Igniters shall be supplied with power from the emergency bus through the breaker to prevent adverse effects on the existing safety systems due to failure of the igniter. In this power distribution scheme, the breaker will be automatically opened to protect the emergency bus when ground fault occurs on the power cable to the igniter.

In addition, cables shall be in accordance with the LOCA specification, considering the characteristics against the environmental condition.

V. Future subject

It is planned to install the equipment for countermeasures against hydrogen combustion as one of the facility improvements required for the accident management strategies. We intend to complete the facility improvements by 2000 and to prepare the guideline for the accident management strategies.

VI. Conclusion

In Japan, countermeasures against hydrogen combustion during a severe accident are taken for the plant with the ice condenser type containment vessel.

We adopted the glow plug type igniter as a hydrogen burning device considering the environmental condition.

We also investigated the hydrogen mixing behavior and increase in the containment vessel internal pressure, assuming the amount of hydrogen generated equivalent to the Zr-water reaction amount at 75 %, based on the S2D scenario.

Igniters are to be installed at the lower part of the compartment, considering the result of experiment conducted by Nupec.

It is planned to install 34 igniters which are equivalent to the double of what required.

Installation of igniters, which is planned as part of the facility improvements required for the accident management strategies, will be completed at latest by 2000.

References

- [1] "Current Status of the Reliability Demonstration Test of Nuclear Power Generation Facilities" p.152, Nuclear Power Engineering Corporation, 1993

Table 1: Experimental Results of Hydrogen Igniters in condition of Severe Accident

item	criteria	catalytic igniter(M)			catalytic igniter(S)			Spark type Igniter (S)			Glow plug type Igniter (T)		
		results	grade	note	results	grade	note	results	grade	note	results	grade	note
Lower Limit of Hydrogen Concentration for Ignition	H ₂ ≤ 8 vol% at Steam Conc. ≤ 55 vol%	~6% H ₂ at 20% Steam 6~7% H ₂ at 40% Steam	×	ignited at 5.4% in good condition (ex. dry). Low reliability of initial ignition	~9% H ₂ at 20% Steam No Ignition at ~10% H ₂ at ≥ 40% Steam	×	ignited at 6-7% in dry condition. Low reliability of initial ignition	~7% H ₂	○		≤ 6% H ₂	○	
Lower Limit of Temperature for Ignition	≤ 10°C	Successful ignition at 10 °C	×	Low reliability of initial ignition	Successful ignition at 10 °C	×	Low reliability of initial ignition (little initial activity at low temperature)	≥ 40°C without poison	×	Ignitable even at 0 °C in the Working Report	No Problem because of extremely high temp. (~1000°C) on the surface of igniter	○	Ignition temp. =730°C →margin=270°C
Delay of Ignition	4~36sec	5-30sec in good condition (ex. dry)	×	Strongly affected by humidity and the number of times of ignition	5-30sec in good condition (ex. dry)	×	Strongly affected by humidity and the number of times of ignition	CaOH makes delay longer up to 276 sec as the experiment goes on.	×	7 sec in the Working Report	Automatic Actuation by Safety Injection signal or CV Isolation signal.	○	
Resistance against Poison	I ₂ , CaOH	Poisoned in the condition of I ₂ 0.05g/m ³ × 24h or CaOH 10g/m ²	×		Poisoned in the condition of I ₂ 0.05g/m ³ × 24h or CaOH 10g/m ²	×		Temperature Switch; failure even over 10% H ₂ in case of I ₂ =0.05g/m ³ × 1h Electrode; Spark rate decreases as experiment goes on in case of CaOH=3.6g/m ³ × 14h	×	It is assumed that CaOH 3.6g/m ³ × 14h should be equivalent to CaOH 10g/m ³ × 5h	Not affected Poisons evaporate from the surface of glow plug because of its high temperature.	○	Zirconia stuck to the surface of plug is simulated by use of crystal glass cover around the plug
Gas Velocity	0.3~5 m/s	Successful ignition in the range of 0.3~5m/s	○		Successful ignition in the range of 0.3~5m/s	○	Another experiment was carried out between 0.1&4.1m/s	ignited at 0.3m/s	○	6m/s in the Working Report. (the lower gas velocity is, the harder ignition occurs)	Surface temperature is higher than ignition temperature even if 5m/s flow	○	Surface Temp. =780°C
Counter-measures against Spray Water		Protected Structurally	○		Protected Structurally (ignited at just below 6% H ₂)	○		Protected Structurally (ignited at just below 8% H ₂)	○		Protected Structurally	○	
SUMMARY		Some items don't meet requirements	×		Some items don't meet requirements	×		Some items don't meet requirements	×		All the items meet requirements	○	Endurance has been proven by TVA and our test.

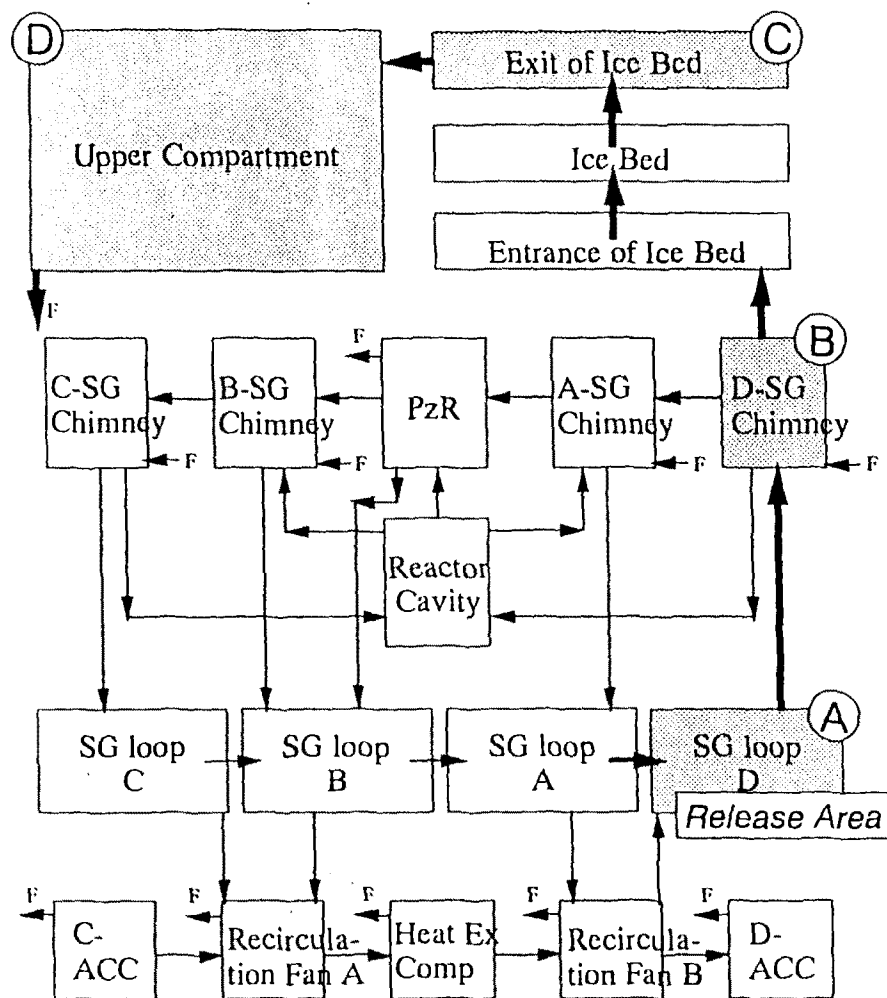
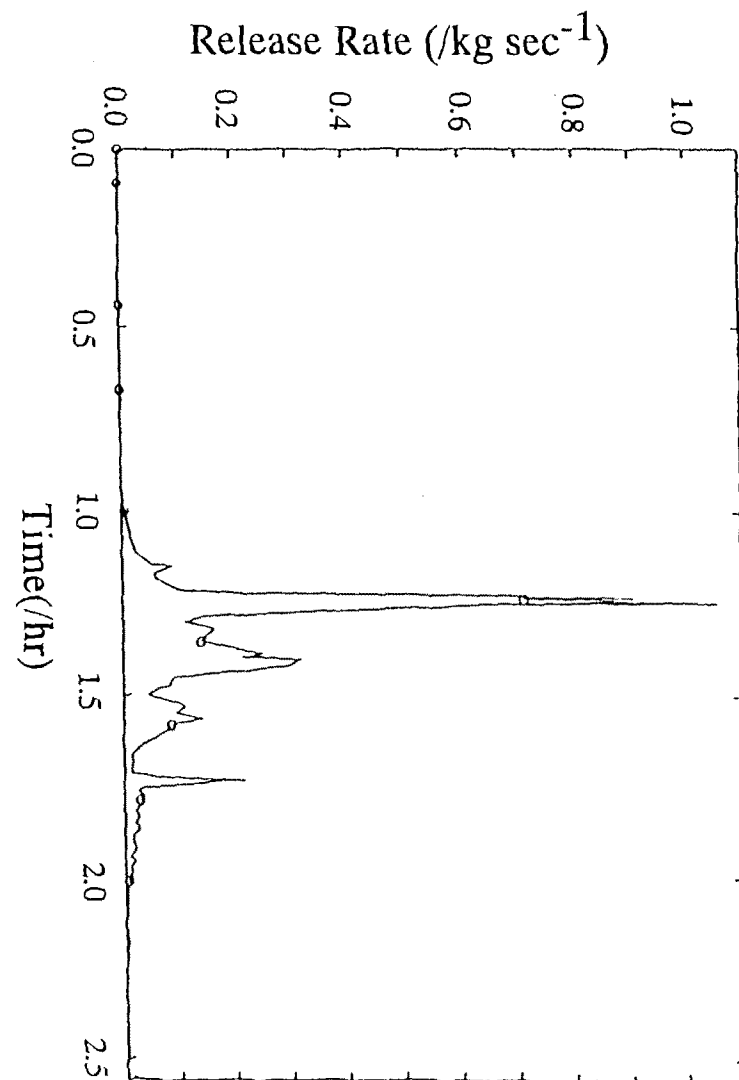


Fig.2: Node-path model of Ohi #1&2 for MAPHY-BURN

Results of H₂ Conc. change for shaded areas are on the graph.
Thickness of arrows implies the flow rate of gas.
"F" means the flow by the air return fan.

Fig.1: Hydrogen Release Rate in case of S2D



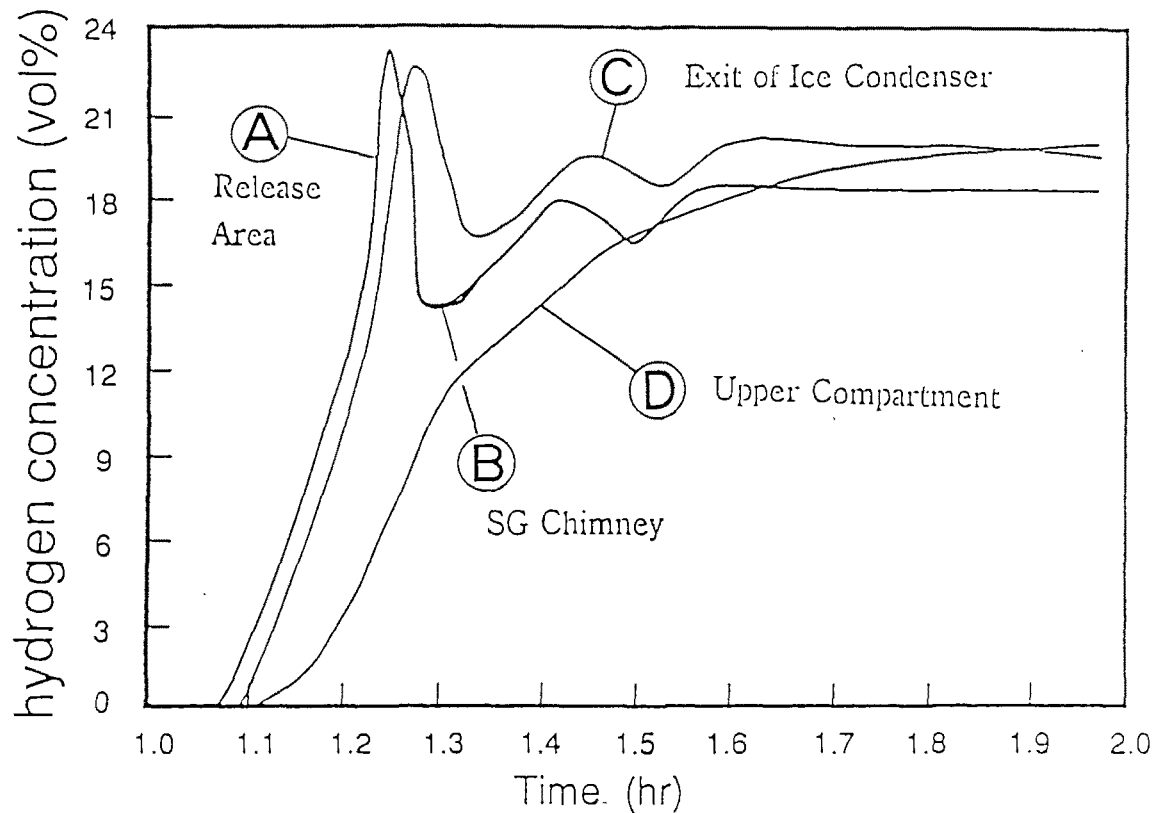


Fig.3 : Mixture of Hydrogen in the Containment

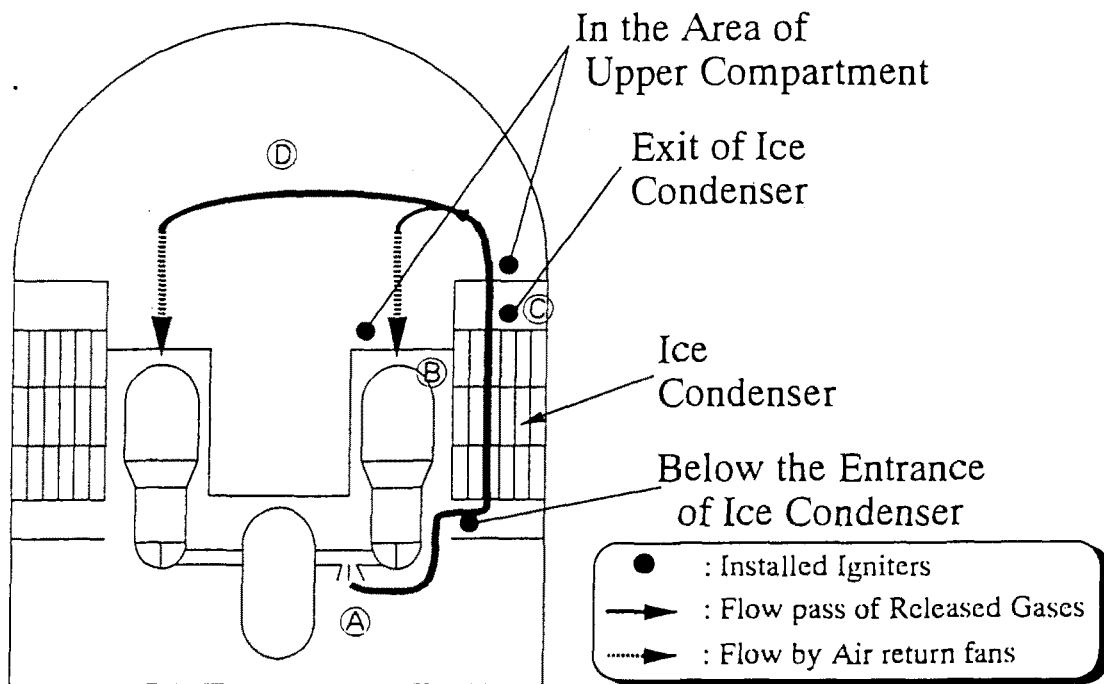


Fig.4 : Location of Igniters in the Containment



3.3 A MECHANISTIC APPROACH TO SAFE IGNITER IMPLEMENTATION FOR HYDROGEN MITIGATION

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Abstract

A new methodology for safe igniter implementation in a full-scale 3-d containment is described. The method consists of the following steps:

- determination of bounding H₂/steam sources,
- high-resolution analysis of the 3-d transport and mixing processes,
- evaluation of the detonation potential at the time of ignition,
- optimization of the igniter system such that only early ignition and non-energetic combustion occurs,
- modelling of the continuous deflagration processes during H₂-release.

The method was implemented into the GASFLOW code. The principle and the feasibility is demonstrated for a single room geometry. A full-scale 3-d reactor case is analyzed without and with deliberate ignition, assuming a severe dry H₂ release sequence (1200 kg). In the unmitigated case significant DDT potential in the whole containment develops, including the possibility of global detonations. The analysis with igniters in different positions predicted deflagration or detonation in the break compartment, depending on the igniter location. Igniter positions were found which lead to early ignition, effective H₂-removal, and negligible pressure loads. The approach can be used to determine number, position and frequency of a safe igniter system for a given large dry containment.

1. INTRODUCTION

1.1 Background

A promising hydrogen control concept in severe accidents is deliberate ignition of reactive mixtures. The main intention is to burn hydrogen early and at low concentrations where only slow combustion can occur. The advantage from the load point of view is that no appreciable pressure rise occurs.

Different igniter systems have been developed and qualified for severe accident environments in various countries [1-9]. Experiments were performed in single room [2-9] or multi-room geometries [10-12], however on scales much smaller than a full-size reactor containment [1-8] and/or without turbulence generating flow obstacles in the test volume [9-12].

This data base was considered sufficient by the USNRC to call for installation of igniter systems in some of the US plant designs (BWR Mark III, PWR ice-condensers). No H₂-countermeasures were required for PWRs with large dry containments. This decision is based on a number of regulatory presumptions:

- only successfully recovered degraded core accidents are considered,
- not more than 75 % of the active cladding can oxidize (about 55 to 60 % of the total Zr inventory),
- containment sprays or fans are always available for mixing,
- the containment failure pressure is 2.5 times the design pressure, and
- early containment failure is tolerated in 10 % of the sequences.

These assumptions lead to well-mixed lean H₂-air-steam mixtures and containment failure probabilities below the 10 % threshold.

The situation in other countries is different due to other regulatory guidelines and other plant designs. For the hydrogen management in existing German plants for instance, more extended sequences with 100 % Zircalloy oxidation must be controlled. The theoretical well-mixed dry H₂-concentration in the containment can reach 20 %. No active systems like fans or sprays for mixing are available. Under these conditions igniter application is characterized by higher hydrogen inventories, more complex geometries, plenty of turbulence generating equipment, H₂-gradients, local enrichments, and higher temperatures. The use of deliberate ignition as a safety-oriented measure under these conditions has been questioned [13]. The main open issue is the predictability of the combustion progress after ignition. What flame speeds and overpressures can be generated? Can fast flames or local detonations develop spontaneously with high pressure loads, missile production and containment damage?

Recent large scale experiments with spark igniters and dynamic H₂-injection into an air-filled obstructed volume [14,15] have identified the relevant processes and confirmed that under such conditions a large spectrum of combustion processes is possible, ranging from benign burn to fully developed detonations. The actual combustion mode depends on the detailed local conditions between source and igniter at the time of ignition. Clearly, for a safety-oriented mitigation system potentially containment threatening events like DDT processes must be eliminated on physical grounds.

The German Reactor Safety Committee consequently asked for additional research in this direction before deliberate ignition could be recommended as a harmless hydrogen mitigation method [16]. It must be shown that igniter systems in real PWR dry containments can eliminate the risk of combustion induced early containment failure.

1.2 Objectives

This paper proposes a mechanistic approach to safe igniter application which does not require the direct numerical simulation of DDT processes during the accident. The approach is based on a DDT criterion which involves a geometrical scaling law, allowing extrapolations to full reactor dimensions. The main steps in deriving a safe igniter system for a given plant are the following:

1. Determine the bounding H₂/steam release scenarios.
2. Perform a detailed 3-d distribution calculation for the given H₂/steam source and containment geometry (GASFLOW code).
3. Select apparently suitable igniter positions.
4. Evaluate the possibility of DDT events by applying a DDT criterion.
 - a) if DDT: improve number, location or frequency of igniters
 - b) if no DDT: acceptable igniter position, model further progress of combustion (standing diffusion flame, slow or fast deflagration).

These steps address the important processes which must be modelled in sufficient detail: H₂/steam release at the break location, subsequent 3-d transport and mixing, combustion mode at first ignition (DDT or not), and further combustion progress in a large complicated geometry.

The theoretical and experimental basis for the used DDT criterion is summarized in Section 2. Section 3 describes the implementation of the DDT criterion in the GASFLOW code, and Section 4 discusses two examples for the determination of acceptable igniter positions.

2. THE DDT CRITERION

2.1 DDT process and effect of scale

The process of spontaneous initiation of a detonation can be divided into two separate phases: (1) the creation of conditions for the onset of detonation by processes of flame acceleration, vorticity production, and formation of jets; (2) the actual formation of the detonation wave itself. Processes in the first phase are scale dependent. They depend on the specific initial and boundary conditions of the problem. However, the second phase, the actual formation of the detonation appears to impose the final scale limitation, which is addressed in the following discussion.

Critical conditions for detonation onset in nonuniform mixtures were studied in [17-19] and certain requirements were derived for the scale of the nonuniformity which is necessary for formation of a detonation in an explosive mixture. These conditions are expressed in terms of local parameters inside the nonuniformity. However, the generation of a local explosion alone is not sufficient for the onset of a detonation. The transmission of the explosion wave to the mixture part outside the nonuniformity should be included in the analysis.

This phase includes the propagation of the explosion wave, which consists of a leading shock and a reaction front, through the region with decreasing reaction rate. During this process, the characteristic width of the reaction zone should grow continuously with decrease of the reaction rate. Increasing regions of reactants should be heated by the leading shock. The associated energy losses can be responsible for the failure of the wave, if the gradient in the reaction rate is too strong. In cases of an expanding wave, additional energy losses are caused by continuous expansion of the wave surface. Finally, the width of the induction and reaction zone should be that of the unperturbed mixture. The larger the characteristic width of the reaction zone of the unperturbed mixture, the larger is the minimum size of the nonuniformity necessary for initiation of a detonation.

The question is what is the minimum volume of a preconditioned mixture so that a local explosion can result in a detonation?

2.2 Scaling parameters

The most important possible parameters for characterizing a mixture sensitivity are

- the reaction zone width l_r ,
- the detonation cell width λ ,
- the critical exit diameter for detonation transmission from a tube to unconfined space,
- the minimum blast initiation energy, and
- the critical tube diameter for single head spin detonation propagation.

Use of the last three parameters is not possible because not enough data are available. Their experimental determination is rather difficult and requires large scale tests for the lean mixtures of interest here. Only l_r and λ can be used at the present time. They both represent characteristic linear scales for a chemical reaction. A value for l_r may be computed using a detailed reaction mechanism and a 1D ZND model. The disadvantages of this parameter compared to the cell width are that (i) it ignores multi-dimensional structure of real detonations, and (ii) the detailed reaction mechanism should be validated itself.

2.3 Numerical modelling

Numerical modelling of the self-initiation phase has been carried out in [20,21]. The aim was to study detonation onset conditions as a function of mixture properties. Two different problems have been analyzed. The first was detonation initiation via autoignition of locally nonuniform mixtures and pressure wave amplification (SWACER mechanism). The initial nonuniformities were formed by spatial temperature distributions and distributions of an additional active chemical component. The second problem was the detonation transmission through a mixture with decreasing reaction rate.

The problem of detonation transition has been considered individually for an one-dimensional planar case. A small high-pressure region was used to initiate the detonation wave. This passed through the region of decreasing reaction rate, which was varied by changing the dependence of the cell length versus distance. An example is discussed in more detail in Appendix A. The possibility of detonation propagation and transmission to the ambient mixture was studied in a number of calculations. It has been found that both the results of the SWACER modelling and the transition study give similar values for the minimum nonuniformity size required for the onset of a spontaneous detonation. This minimum scale for detonation formation can be estimated as 7λ in terms of the cell width [21,22]. This estimation corresponds to the planar case, optimum shape and nature of the nonuniformity. In the case of expanding waves (spherical symmetry), the minimum scale of nonuniformity increases up to 30λ due to additional requirements for the minimum curvature of the wave.

Also analytical 1-d calculations for detonation propagation through sensitivity gradients confirmed that such cell size scaling correlations should be applicable. Additional calculations have shown that the estimate of 7λ for minimum

size of a sensitized mixture should also be valid for mixtures with different effective activation energies.

2.4 Experimental results

The minimum scale requirement for the onset of a detonation provides a basis for the comparison of different experimental results [23]. Such a comparison is presented in Fig. 1, as a correlation between the nonuniformity size L and the detonation cell width λ . Experimental results on both detonation initiation by a turbulent jet of hot combustion products, and on deflagration to detonation transition have been collected. Experimental data on cell width were used [24,25]. For the turbulent jet initiation experiments, the jet orifice diameter was used as the characteristic size L . For DDT experiments this is the linear size of the volume ($V^{1/3}$), which controls the possible macroscopic nonuniformity size. The results of the premixed RUT experiments represent the largest scale and the least sensitive mixtures. The shown line describes the relation $L = 7 \lambda$, described above.

Because the scale limitation represents a necessary but not sufficient condition, no sharp separation can be expected between the deflagration and detonation modes. For initial conditions below the line the minimum scale requirement is satisfied, but this does not mean that detonations must be initiated in all cases. Other conditions concerning the early stages of the DDT must also be met. The important point is that a detonation should not occur in the upper part of Fig. 1, due to the scale limitation.

The accuracy of the cell width data is within a factor of 2. Furthermore, the minimum scale requirement $L = 7 \lambda$ itself is only approximative. Nevertheless, the data in Fig. 1 show clearly the effect of scale within these limits of accuracy. The proposed scale limitation $L = 7 \lambda$ is in a good agreement with the experimental results over a wide range of volumes and H_2 -air mixture compositions.

Several series of experiments with dynamic hydrogen injection and spark ignition have been carried out in the RUT facility [14,15]. The objective of the tests was to study possible consequences of deliberate ignition in dynamic conditions. The experimental variables were igniter location, ignition time, injection rate and injection location.

The possibility of DDT was found to depend only on the mean hydrogen concentration in the cloud for the given size of the enclosure. If the detonation cell width is defined by the mean hydrogen concentration in the cloud, and the cloud size itself gives the characteristic nonuniformity size, we can analyze the data of the dynamic tests in the same way as those of premixed experiments. Fig. 1 shows that the 7λ criterion can be applied for dynamic as well as for premixed initial conditions.

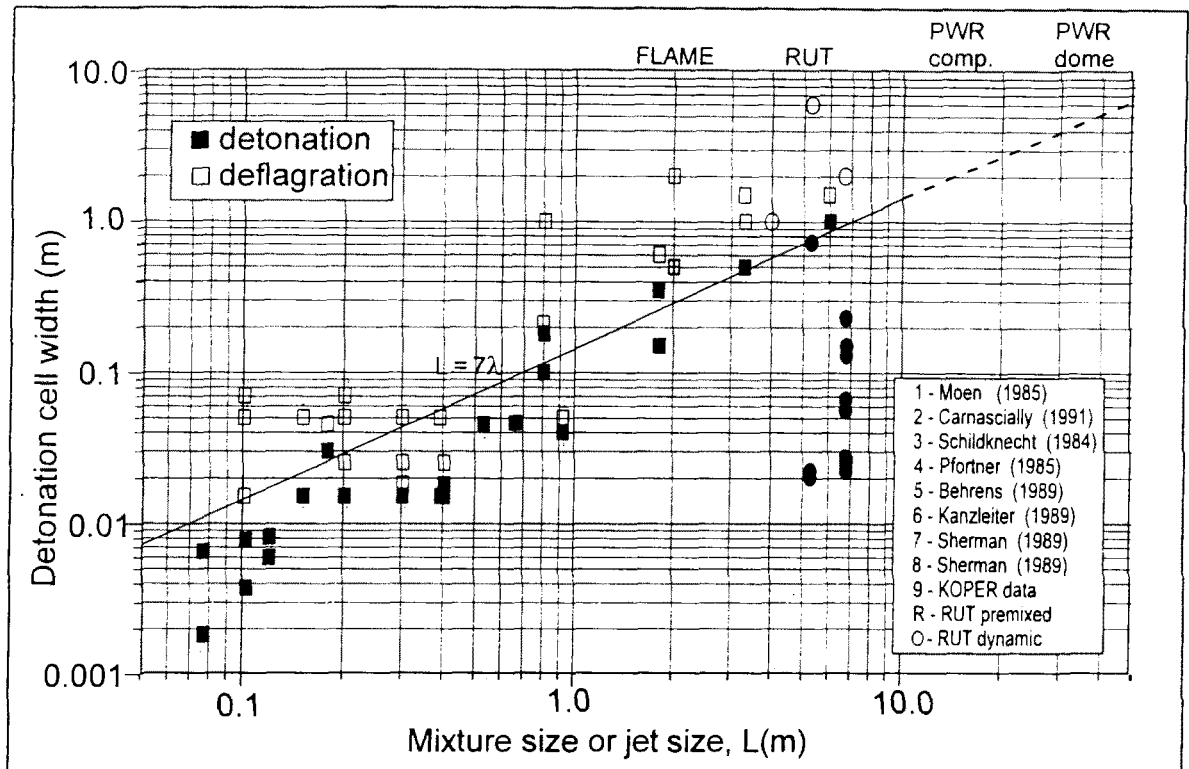


Figure 1 Experimental data on turbulent jet initiation and DDT in confined volumes.

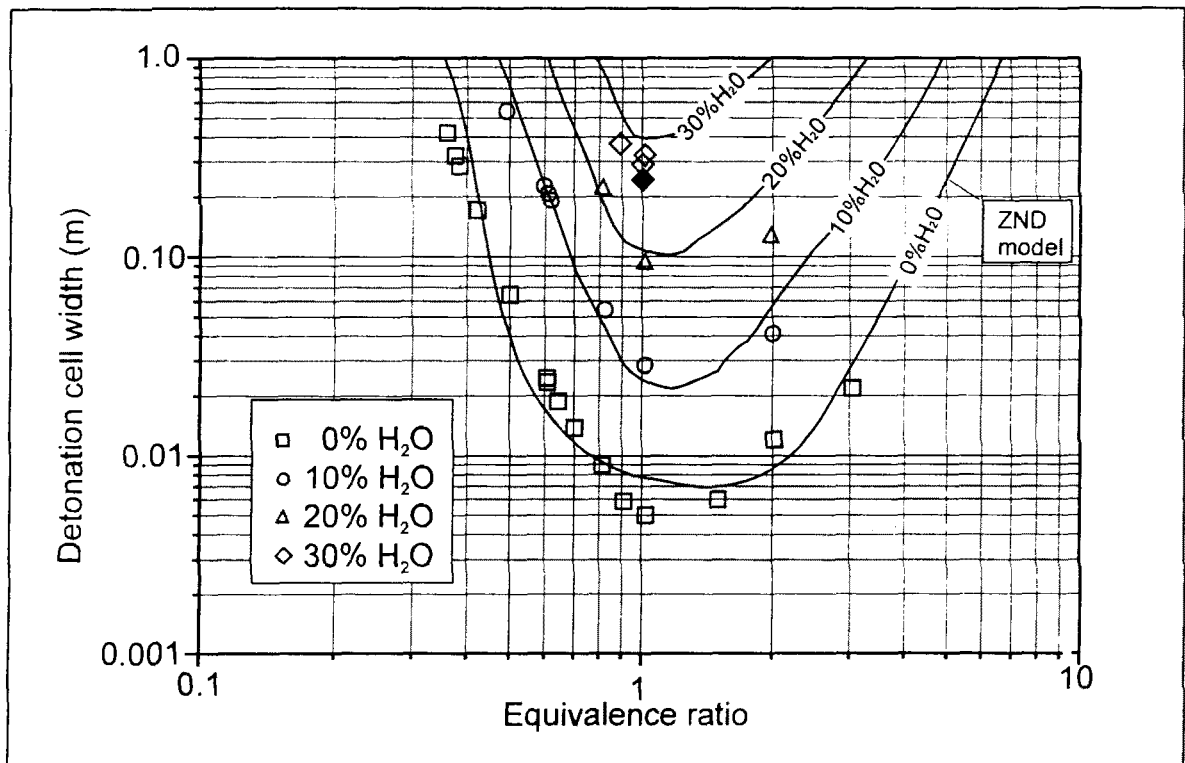


Figure 2 Measured and calculated detonation cell width of H_2 -air-steam mixtures [25] for 100 °C and constant air density (41.6 mole/m³).

3. NUMERICAL MODEL

The above described DDT criterion was implemented into the 3-d field code GASFLOW, which has been described recently [26-28]. The methodology proceeds as follows.

Step 1: Characteristic cloud dimension L .

The characteristic dimension $L_n(t)$ of the H_2 -air-steam cloud in room number n which evolves from the source location is calculated from

$$L_n(t) = V_n(t)^{1/3} \quad (1)$$

$$V_n(t) = \sum_i \Delta V_{i,n}(t) \quad (2)$$

where $\Delta V_{i,n}$ are those computational cells in containment room n , which contain a burnable mixture at time t . In case of a dry H_2 -air mixture this are the grid cells containing between 4 % and 75 % H_2 , the lower and upper flammability limits, respectively.

Step 2: Average detonation cell width λ .

The average composition of the H_2 -air-steam cloud in room n at time t is

$$[\bar{x}_{H_2}(t)]_n = (\sum x_{H_2,i} \cdot \Delta V_i)_n / V_n \quad (3)$$

$$[\bar{x}_{H_2O}(t)]_n = (\sum x_{H_2O,i} \cdot \Delta V_i)_n / V_n \quad (4)$$

where $x_{H_2,i}$ = hydrogen volume fraction in cell i of the H_2 -air-steam cloud in room n
 $x_{H_2O,i}$ = corresponding steam volume fraction.

This average composition is used to calculate the average equivalence ratio of the cloud which in case of H_2 -air-steam mixtures is

$$\phi_n = 2.3866 \bar{x}_{H_2,n} / (1 - \bar{x}_{H_2,n} - \bar{x}_{H_2O,n}) \quad (5)$$

The average detonation cell width λ_n of the cloud mixture in room n can now be evaluated from measured data for $\lambda_n(\phi, \bar{x}_{H_2O})$, e.g. Fig. 2. The average composition and detonation cell size of the cloud is used here as measure for the detonation sensitivity because this evaluation method gave good agreement with the 7λ -correlation in the RUT tests with dynamic H_2 injection into air (circles in Fig. 1).

Step 3: DDT index R .

At any given time during the calculation a DDT index R is evaluated for room n according to

$$R_n(t) = \frac{L_n(t)}{7 \lambda_n(t)} \quad (6)$$

If this ratio is less than 1 at the time of ignition t_{ign} , then the igniter is located in an acceptable position in that particular room n . If the ratio R_n is larger than 1 at the time of ignition the ignitor is located in a not acceptable position. In this case a DDT cannot be excluded, according to the above described criterion. It is important to note that this criterion includes the different experimentally observed types of detonation transition, like e.g. accelerating flames, jet ignition, normal reflection, or focussing of pressure waves in corners.

4. APPLICATIONS

The described methodology is first illustrated for a single room geometry and then applied to a full scale 3-d multi-compartment reactor containment.

4.1 Battelle Model Containment

Hydrogen is injected into one closed room of the Battelle Model Containment (Fig. 3). The room contains initially dry air at 1 bar pressure and 300 K. The vertical H₂-jet enters the room at the center of the floor, the H₂-gas being at the same initial conditions (1 bar, 300 K, 21.6 g H₂/s, velocity 1.0 m/s). Two cases will be discussed: a calculation with early ignition ($R < 1$), and a calculation with late ignition ($R > 1$).

4.1.1 Early ignition

A GASFLOW analysis was performed with a glow plug ignitor in the upper corner of the BMC room (Fig. 3). This high location should cause an early ignition because buoyancy forces support the transport into this direction.

The cloud dimension grows monotonically with time. At 12 seconds the flammable edge of the H₂-air cloud reaches the ignitor which initiates a burn. The flame attaches to the source, reducing the cloud diameter of unburned gas to one or two computational cells. The hydrogen inventory in the room drops rapidly due to the burn. The DDT index R initially shows large values when only few cells above the release location are filled with a rich H₂ mixture. R then decreases quickly and it was well below 1 at the time of ignition, so that no DDT potential should have existed. The fluctuations in R during the burn are due to the relatively rich but small standing diffusion flame. This diffusion flame does not represent a DDT threat.

The gas temperatures above the standing flame reach high values during the full burning period. To reduce thermal loads it is advisable to place igniters high, but not directly above potential H₂ release locations. The calculated (uniform) pressure in the BMC room depicts the phases of H₂-injection, sudden burn-out of the accumulated hydrogen, and continuous burn of the injected H₂.

4.1.2 Late ignition

The second calculation involved an ignitor at the worst possible location, namely at the lowest and farthest position from the source (Fig. 4). Hydrogen reaches this point only after the room is completely filled by the H₂-air cloud. At ignition time the cloud dimension L is equal to the third root of the room volume $[(41 \text{ m}^3)^{1/3} = 3.44 \text{ m}]$. The calculated DDT index is well above 1. Almost 800 g of hydrogen burn out after ignition. The current chemical kinetics model in GASFLOW is not intended to describe fast turbulent combustion and DDT but treats the combustion as a slow diffusion controlled deflagration. Fig. 4 shows the calculated pressure and temperature increase.

The DDT criterion predicts that in this late ignition the deflagration could well develop into a local detonation [$R(t_{\text{ign}}) \approx 10$]. At the time of ignition the hydrogen concentration at the ceiling had reached 40 %, a highly sensitive H₂-air mixture.



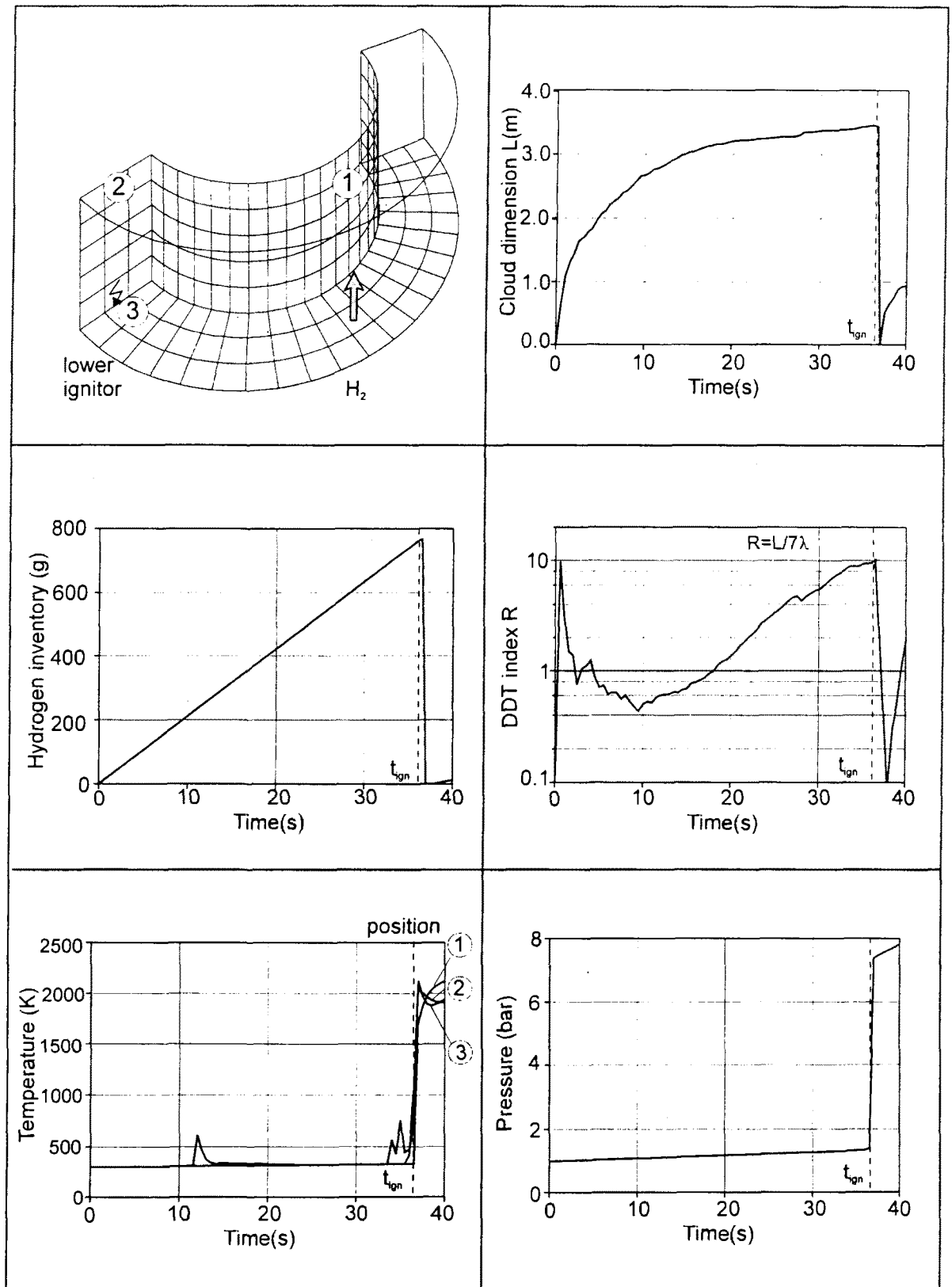


Figure 4 Results for H_2 injection into BMC room with igniter in lower corner (late ignition).

4.2 Reactor containment

Two types of calculations were performed for a future 3-d reactor containment with 90000 m³ free volume:

- a transport analysis with evaluation of the detonation potential, and
- a transport and combustion analysis with deliberate ignition.

The containment geometry is shown in Fig. 5. A 180 degree symmetry was assumed. The main structures and components are modelled with about 12000 computational cells. Hydrogen is released through an internal water storage tank (IRWST) according to Fig. 6. In this hypothetical release scenario the first major peak corresponds to the early core degradation process and the second large peak could be due to a partial reflood of the molten core, similar to the TMI case. The release curve contains peaks of different magnitudes ($\approx 2, 0.8, 0.2$ kg/s). The maximum value of 2 kg/s, the total mass of 1200 kg, and the assumptions of a dry H₂-source and containment atmosphere represent very severe conditions. This case was investigated to explore the limits of the deliberate ignition concept.

4.2.1 Transport analysis

A 3-d transport analysis was performed for the described scenario to evaluate the DDT potential without mitigation measures. The evolution of the hydrogen distribution is characterized by calculating the H₂-cloud dimensions L and the corresponding DDT index R in the following six rooms of the containment: IRWST, pump rooms 1 and 2, steam generator rooms 1 and 2, and containment dome.

The results are presented in Figures 7 and 8. During the first release phase (up to 2000 seconds) hydrogen is transported into all the rooms. The characteristic cloud dimension reaches in all cases the respective geometrical limit, namely the cube root of the room volume, e.g. 30 m for the dome. This means that a burnable mixture exists in all containment regions. Fig. 8 shows the calculated detonation potential which decreases with the distance from the source (0 - 2000 seconds). Very high DDT potential exists in the IRWST over long periods of time. In the first pump room local detonations are only possible during the first main release peak, and not at all in the remaining rooms.

A different situation evolves during the second release phase (2000 - 4000 seconds). The mixture in the IRWST is even more reactive because now the atmosphere, which enters the IRWST by natural convection, already contains a background level of hydrogen. The detonation potential in all other rooms increases with increasing hydrogen release in a similar way. At 4000 seconds a significant DDT possibility exists throughout the containment ($R > 1$ in all rooms). In the containment dome the possibility of a global detonation is predicted. The stepwise increase of $R(t)$ in the dome corresponds closely to the released H₂ mass (Fig. 6). The DDT potential in the containment dome reacts in an integral manner to the H₂-release rate, whereas rooms close to the source follow the release rate directly.

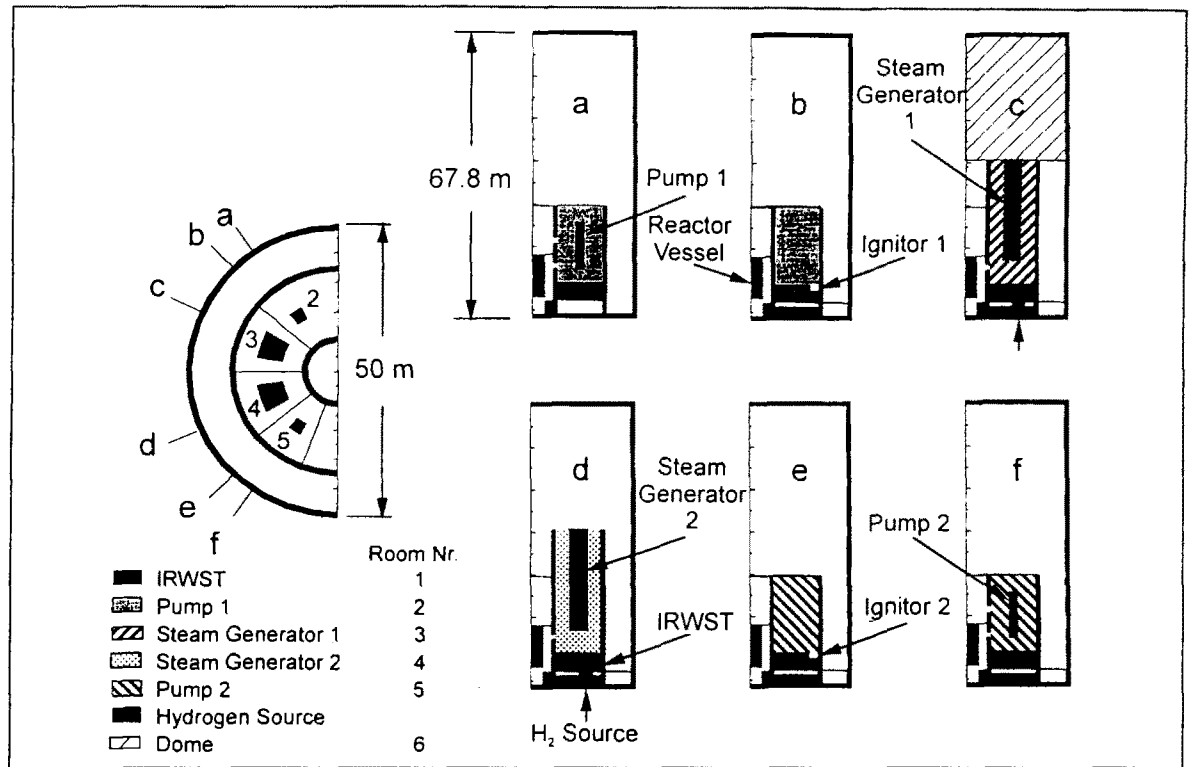


Figure 5 GASFLOW 3-d geometry model for full scale containment.

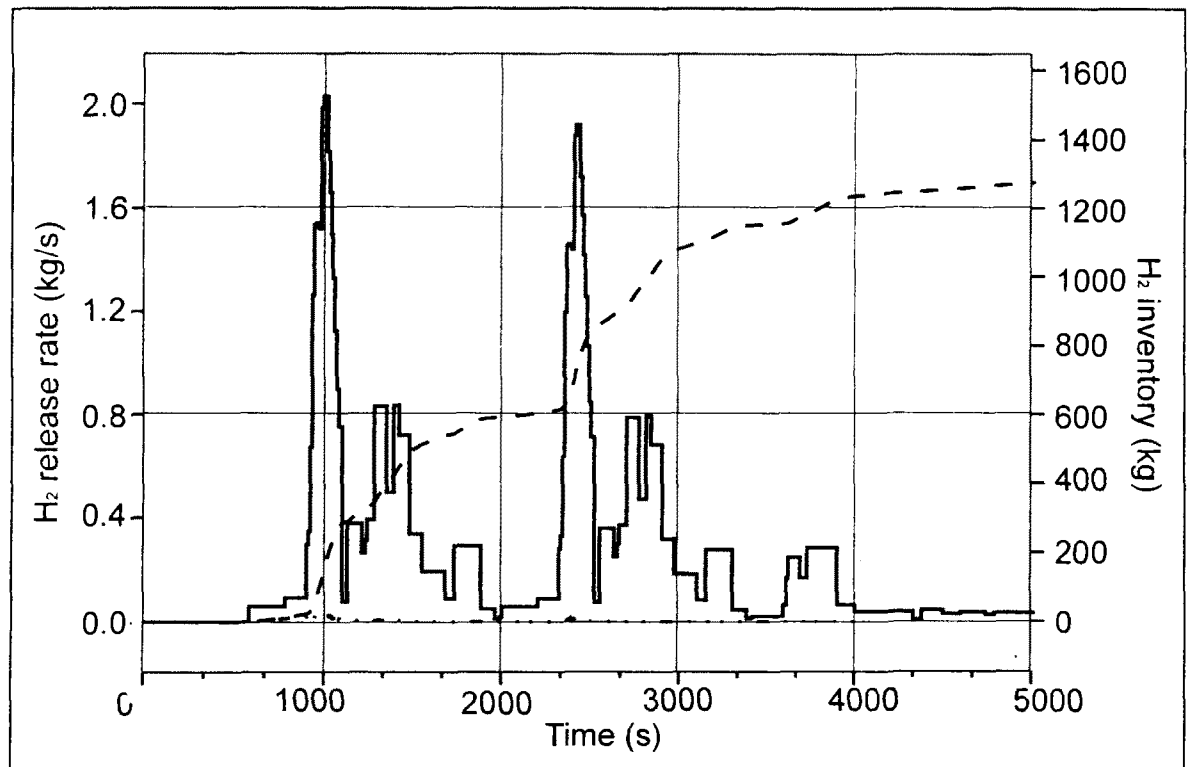


Figure 6 Hydrogen release in the analyzed dry release scenario and resulting H₂ inventory without and with deliberate ignition.

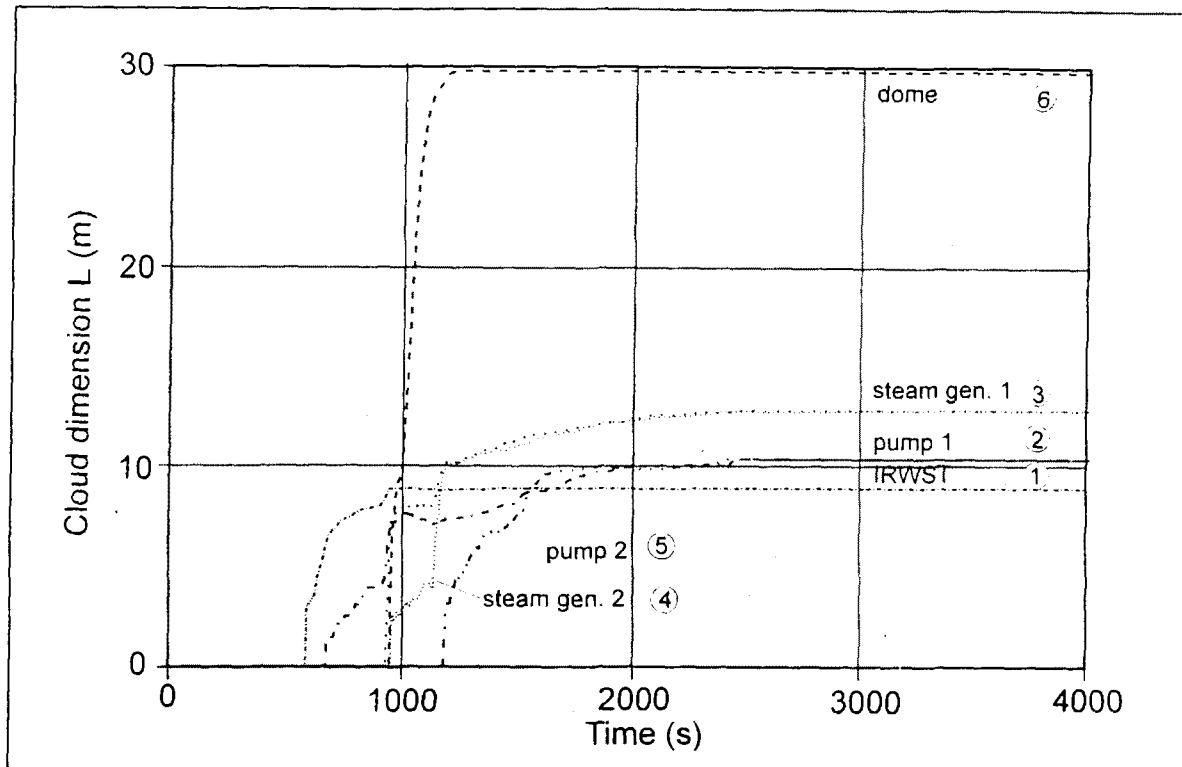


Figure 7 Computed cloud dimension for six rooms of the containment.

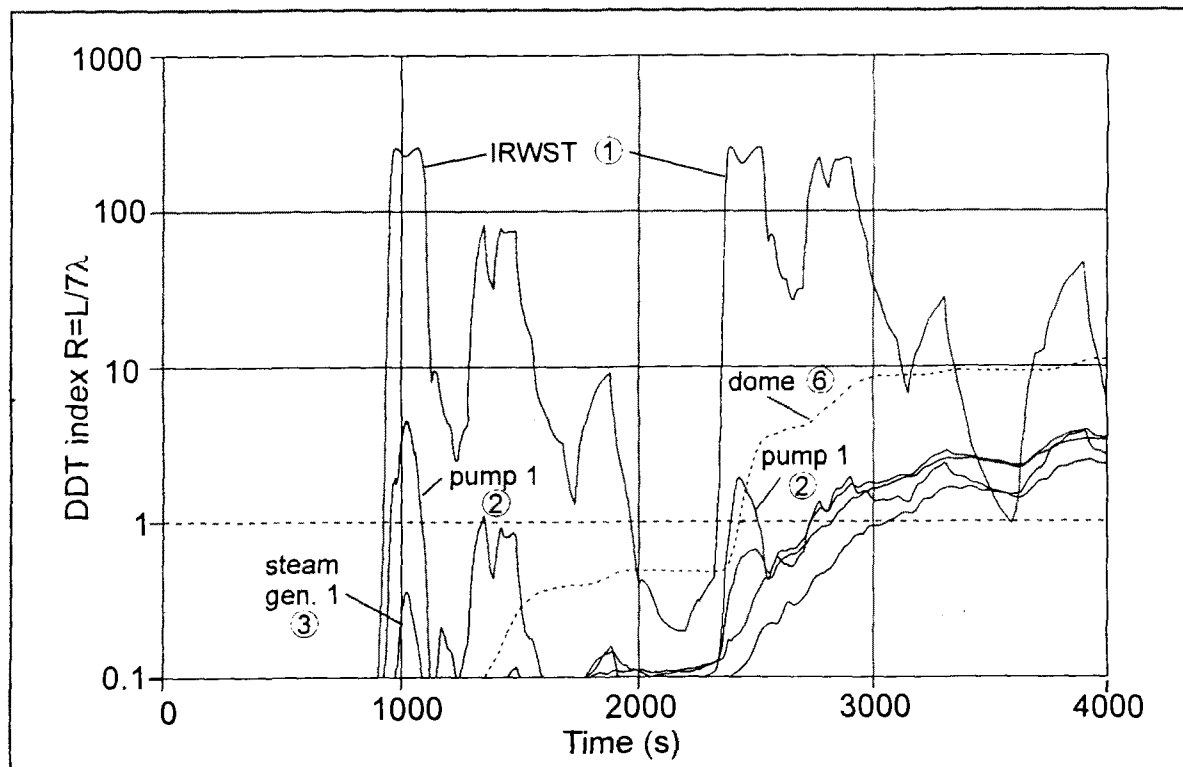


Figure 8 Computed detonation potential for six rooms of the containment.

Fig 8 also provides information on the typical time constants needed to change from the deflagration regime ($R < 1$) to the potential detonation regime ($R > 1$). Generally this time constant increases with the distance from the source, but it is relatively short, typically 50 to 500 seconds. The second major release peak drives the large dome into DDT conditions within only 100 seconds. These times are significantly shorter than practicable H_2 removal times with recombiners (thousands of seconds). The present calculation quantifies the well-known concern that recombiners alone may not be sufficient as H_2 -control system.

The presented example demonstrates that the proposed approach allows to quantify the DDT potential in a 3-d containment during a given H_2 /steam release scenario by calculating a DDT index R as function of time and space. The results clearly show the need for a reliable and fast acting hydrogen management system in such dry containments equipped with large PWR cores.

4.2.2 Analysis with deliberate ignition

The above described unmitigated sequence was analyzed with spark igniters at different positions in the IRWST (Table 1).

Table 1. Analyzed deliberate ignition cases and results.

Case	location	Spark igniter intervall (s)	t_{ign} (s)	$R(t_{ign})$	H_2 -inventory at t_{ign} (kg)
1 - B0	at IRWST openings to pump rooms	10	944	24	18
2-C4	as case 1	1	941	13	16.6
3-C2	at IRWST ceiling, 3 m above source	10	914	0.5	10.5
4-C3	2 m above H_2 -source	10	912	0.1	10.2

Fig. 9 shows a 3-d view of the investigated geometry. The grey volume below the igniters represents the calculated flame shape after the flame traveled from the igniters towards the H_2 -sources (case 3-C2, 922 s). The flame later expands throughout the IRWST and remains attached to the H_2 -sources. The later combustion in the IRWST is oxygen limited by the air inflowing from the right opening. A hot H_2/N_2 mixture leaves the IRWST through the left opening and burns on first contact with air.

For discussion of the results it is helpful to distinguish two time periods in the sequence:

1. a low release phase from 600 - 900 s with an average H_2 -release rate of $\dot{m}_{H_2} = 0.04$ kg/s (Fig. 6),
 2. the first release peak, 900 - 1000 s, with $\dot{m}_{H_2} = 0.04 \rightarrow 1$ kg/s.
- (All H_2 -masses in the GASFLOW calculation are half of the total release because of the assumed 180 degree symmetry.)

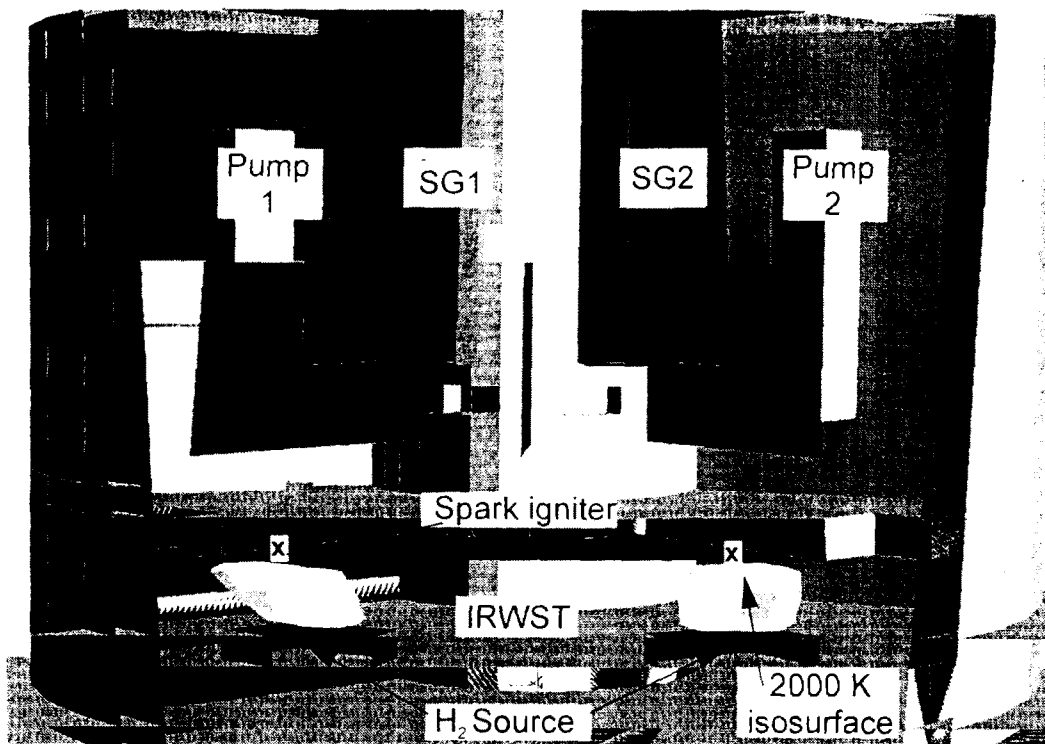


Figure 9 GASFLOW analysis of a dry release scenario with deliberate ignition. The flame travelled back from the igniters to the H₂-sources (case 3-C2, 922 s).

During the first period about 500 m³ of the IRWST are filled with a lean H₂-air mixture (cloud size 8 m). The H₂-concentration remains constantly between 4 and 5 %, which is the flammability limit for upwards propagation. Hydrogen above this value is removed by small local, multiple burns at the igniter location (3 to 4 kg H₂ up to 900 s). This initial situation was very similar for all investigated igniter positions.

In the second time period (900 - 1000 s) the H₂ release rate increases rapidly from the low value to about 1 kg H₂/s. The burnable cloud now expands from about 500 m³ volume ($L \approx 8$ m) to 718 m³ ($L = 8.9$ m) which is the total IRWST volume in the calculation. More importantly the mixture sensitivity now increases rapidly, the detonation cell size λ decreases, and the DDT index R increases. The critical value of $R = 1$ is reached at 928 seconds.

The further events depend on the "time of ignition", the time at which the flame propagates away from the igniter towards the source the first time. In two of the investigated cases the ignition occurred when R was above 1 (later than 928 s) and in two cases when R was below 1 (before 928 s). The hydrogen inventories in the IRWST at the ignition time were between 10.2 and 18 kg (Table 1), which corresponds to a very insensitive and a quite reactive mixture, respectively.

The full burn-out of the IRWST requires two necessary conditions, namely that the edge of the burnable cloud has arrived at the nearest igniter, and that the H₂-concentration is high enough for free flame propagation. With the present release scenario and igniter positions the time of ignition was determined by

the second reason, the H_2 concentration necessary for downward propagation (8 - 9 % H_2).

In summary the calculations have shown that very severe release sequences can be controlled with correctly located spark igniters. The appropriate locations depend on details of the geometry and the source, and they cannot be inferred from scaled down model experiments.

A purposely conservative situation was investigated here to explore the limits of the deliberate ignition concept. Dry hydrogen was injected with high rates and multiple release peaks into a dry containment atmosphere. This is the most reactive case possible. In all mechanistic sequences steam will be present and lower the mixture sensitivity. If e.g. the IRWST water would be at elevated temperatures a significant steam source would be present ($p_{sat} = 0.5$ bar for $80^\circ C$). A corresponding "sump model" to describe water evaporation and transport in GASFLOW is currently under development. For the lean mixture of interest here, the addition of e.g. 20 % steam, would increase the detonation cell width by a factor of about 30 (Fig. 2). The value of R would therefore decrease by the same factor, and all cases in Table 1 would become acceptable ignitor positions ($R < 1$). (The ignition time would be similar because the downward flammability limit depends only weakly on the steam concentration.)

Thermal loads are another topic that needs to be addressed in future analyses for igniter positioning. GASFLOW predicted standing diffusion flames in all investigated cases, which produced significant temperatures in the vicinity over extended periods of time. These regions can be identified in the simulations and should be excluded as potential igniter locations.

5. CONCLUSIONS

This paper proposes a new mechanistic approach to safe igniter implementation which includes determination of bounding H_2 /steam release sequences, high-resolution 3-d analyses of transport and mixing, selection of apparently suitable igniter positions, evaluation of the detonation potential at the time of first ignition, and modelling of the further deflagration modes. The used DDT criterion ($L = 7 \lambda$) is based on theoretical and experimental work. The data cover a sufficiently large range of scales to justify extrapolation to the full plant size (Fig. 1).

The methodology was implemented into the GASFLOW code and demonstrated for a single room of the BMC by analyzing early and late ignition of the spreading H_2 -air cloud. The high igniter position leads to early ignition (DDT index $R < 1$). The low igniter position leads to late ignition of an enriched H_2 -air cloud and high DDT possibility ($R > 1$).

Calculations were performed for a future full-scale 3-d reactor containment, without and with deliberate ignition. To explore the limits of the deliberate ignition concept a very severe dry release sequence was investigated.

The unmitigated case provided quantitative information about the time and space dependency of the detonation potential. During the first release phase (total of 600 kg H_2) DDT possibilities exist only near the release location. The second release phase with additional 600 kg H_2 leads to significant DDT poten-

tial throughout the whole containment, including global detonations in the dome. The times required to change from the deflagration regime ($R < 1$) to the potential detonation regime ($R > 1$) can be significantly shorter than typical recombiner time constants. Recombiners alone are not sufficient as a H_2 -control system for fast sequences similar to the analyzed one. CO_2 dilution must be accomplished before the first H_2 -release begins, to be an effective H_2 control measure.

The GASFLOW analysis of the same sequence with different igniter positions predicted that it is possible to control even severe dry release conditions with appropriately located igniters. The first burn-out of the release compartment (IRWST) could be triggered early enough before DDT possibilities had developed. Very effective H_2 -removal was predicted for the whole release sequence without damaging pressure loads. The existence of steam largely decreases DDT possibilities because the detonation cell size λ increases rapidly with the steam concentration (Fig. 2).

The outlined methodology has been realized in the GASFLOW code. The current model verification covers the sequence of events up to the first burn-out of the release compartment, where either the detonation regime ($R > 1$) or the deflagration regime ($R < 1$) is entered. Models are under development to describe the further progress of the deflagration as attached diffusion flame, freely propagating slow or turbulent flame.

Appendix A

An example for the successful transmission of a local detonation from a sensitive region to a less reactive gas mixture is presented in Fig. A1 [29]. The one-dimensional planar calculation considers a mixture gradient with increasing detonation cell width. The most reactive mixture is located at $x = 0$ and the sensitivity decreases with increasing x up to about 180 mm, from where on the mixture has a constant cell width λ_0 of about 24 mm. This λ_0 value corresponds e.g. to approximately 20 % H_2 in air at 1 bar and 375 K, or to 30 % H_2 and 10 % steam in air at the same pressure and temperature conditions.

A small high pressure region was used to start the detonation at $x = 0$. The evolving pressure profiles are shown for three times: first inside the sensitive region (t_1), then shortly after propagation into the unperturbed mixture (t_2), and finally after achieving the stable self-similar profile in the homogeneous mixture (t_3).

The linear extension of the sensitive region was slightly larger than 7 times λ_0 , the detonation cell width of the unperturbed mixture. The detonation decayed in calculations with smaller sensitive regions.

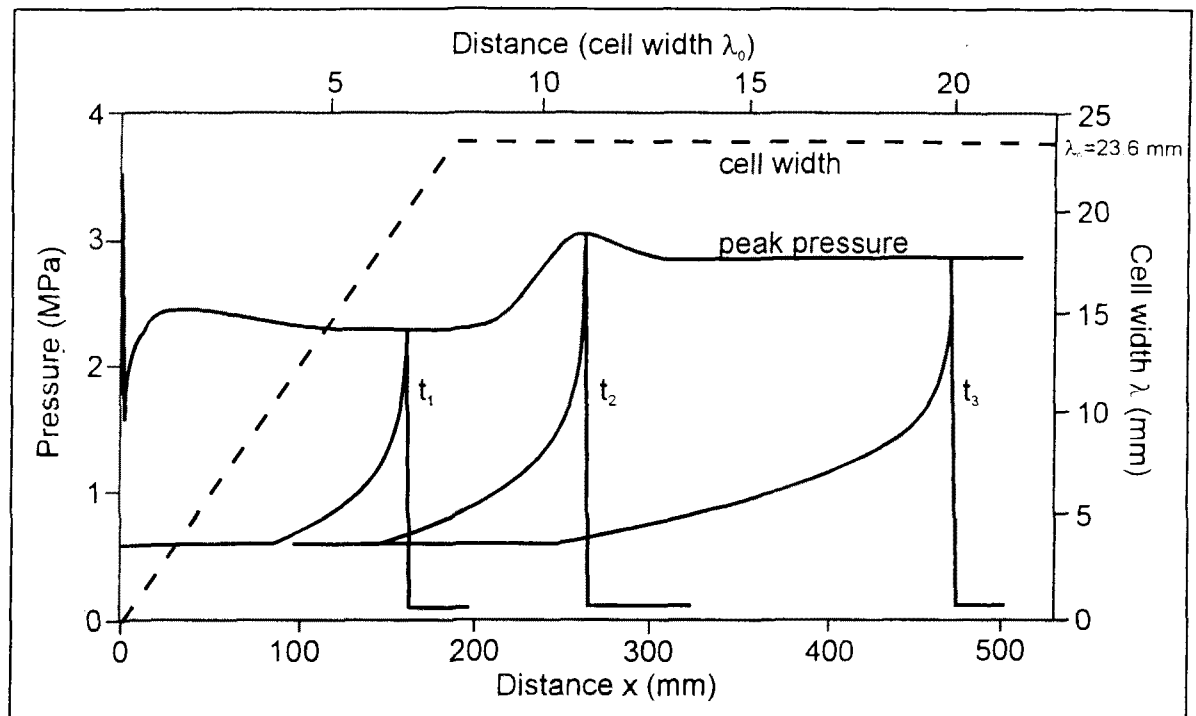


Fig. A1 Example for calculated planar detonation transmission from a sensitive region ($x < 180$ mm) to an unperturbed mixture ($x > 180$ mm).

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3.4 THE LARGE-SCALE VENTED COMBUSTION TEST FACILITY AT AECL-WL: DESCRIPTION AND PRELIMINARY TEST RESULTS

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Abstract

Implementation of hydrogen mitigation systems in nuclear reactor containments requires testing the effectiveness of the mitigation system, reliability and availability of the hardware, potential consequences of its use and the technical basis for hardware placement, on a meaningful scale. Similarly, the development and validation of containment codes used in nuclear reactor safety analysis require detailed combustion data from medium- and large-scale facilities. A Large-Scale Combustion Test Facility measuring 10 m x 4 m x 3 m (volume, 120 m³) has been constructed and commissioned at Whiteshell Laboratories to perform a wide variety of combustion experiments. The facility is designed to be versatile so that many geometrical configurations can be achieved. The facility incorporates extensive capabilities for instrumentation and high speed data acquisition, on-line gas sampling and analysis. Other features of the facility include operation at elevated temperatures up to 150°C, easy access to the interior, and remote operation. Initial thermodynamic conditions in the facility can be controlled to within 0.1 vol% of constituent gases. The first series of experiments examined vented combustion in the full 120 m³-volume configuration with vent areas in the range of 0.56 to 2.24 m². The experiments were performed at ~27°C and near-atmospheric pressures, with hydrogen concentrations in the range of 8 to 12% by volume. This paper describes the Large-Scale Vented Combustion Test Facility and preliminary results from the first series of experiments.

Introduction

Implementation of hydrogen mitigation systems in nuclear reactor containments requires testing the effectiveness of the mitigation system, reliability and availability of the hardware, potential consequences of its use and the technical basis for hardware placement, on a meaningful scale. Similarly, development and validation of many existing containment codes used in nuclear reactor safety analysis require detailed combustion data from medium- and large-scale facilities. To address combustion issues rising from hydrogen production and release, experimental work in medium- and large-

scale facilities have been in progress in many organizations. Examples of work done in the medium-scale facilities include combustion experiments in the 6.3-m³ and 10.3-m³ Containment Test Facility (CTF) vessels at Whiteshell Laboratories, and the 5-m³ VGES and 5.6-m³ FITS cylindrical vessels at Sandia Laboratories [1-4]. Examples of work done in large-scale facilities include combustion experiments in the Battelle Model Containment [5], the HDR facility [6], and the 15.85-m diameter spherical vessel located at the Nevada Test Site (NTS) [7]. Recently, NUPEC has performed hydrogen combustion experiments in their 1/6-scale containment vessel.

In nuclear reactors equipped with deliberate ignition systems, a global hydrogen burn is unlikely. It is more likely that combustion will be initiated in a particular subvolume in the vicinity of the release where flammable mixtures first arise. Overpressures generated by combustion would be relieved by venting to adjacent compartments that contain no combustible gases via existing openings. Credit for the pressure relief by venting is used in the analysis of the integrity of these compartments.

At AECL Whiteshell Laboratories, vented combustion experiments were previously performed in the intermediate-scale 2.3-m diameter CTF sphere. However, the full range of vent ratios (vent area/[vessel volume]^{2/3}) of interest (0.1 to 2) could not be achieved. Moreover, questions regarding the effects of size and geometry of the enclosure were not entirely resolved. Design of a new facility was therefore undertaken to address these issues. The requirements for the new Large-Scale Vented Combustion Test Facility (LSVCTF) were:

- accurate control of initial thermodynamic conditions,
- instrumentation capability for validation of 3-D codes,
- variable geometric configuration,
- geometric similarity to actual rooms,
- short duty cycle, and
- easy access to the combustion chamber interior.

This paper describes the LSVCTF at AECL Whiteshell Laboratories, its capabilities, and results of some recent combustion experiments performed in the facility.

Description of the Facility and Instrumentation

Facility Description

Figure 1 shows a schematic of the facility. The LSVCTF is a 10-m long, 4-m wide, 3-m high rectangular enclosure with an internal volume of 120 m³. It is constructed of 1.25-cm thick steel plates welded to a rigid frame work of steel I-beams. The entire structure is anchored to a 1-m thick concrete pad. Two roller-mounted movable end walls are provided to open up the vessel for internal modifications or to move-in bulky experimental equipment when needed. The whole facility, including the end walls is electrically trace-heated and heavily insulated to maintain temperatures in excess of

100°C for extended periods of time. The entire combustion chamber is enclosed in an insulated metal quonset (see Fig. 2), which houses the gas analysis and hydraulic fan systems on one side and all the valves and piping on the other side.

The end walls are covered with rectangular steel plates measuring 0.37 m by 0.74 m bolted to the end wall structure. The vent area can be changed by removing or replacing the appropriate number of panels.

The combustion chamber can be subdivided into 2 or 3 compartments using structural steel partitions. These partitions also have openings to allow internal venting. The partitions can be installed or removed in about one day.

A large number of penetrations are provided for mounting the required instrumentation in the vessel. These provide a means of varying the transducer locations for optimum transducer response.

Eight hydraulic fans, four on each side wall, are installed in the combustion chamber to mix the gases uniformly.

The facility is located in a fenced area and is remotely operated to ensure operator safety.

Instrumentation

Pressure and Temperature Measurement

The facility is designed to accommodate extensive instrumentation. Up to 144 channels of transient temperature and pressure data can be obtained at sampling rates ranging from 10 to 100 kHz, making it possible to acquire data from a variety of combustion experiments; from slow recombiner tests that last over several hours to fast turbulent vented deflagration tests that only last for several hundred milliseconds.

A schematic of the instrumentation employed in the present series of experiments is shown in Fig. 3. Transient pressures in the vessel were measured by six Kulite HEM-375 and XTME-190-high temperature pressure transducers. Because of the long combustion times, the transducers had to be protected from thermal loading. This was done by recess-mounting the transducers. Additional thermal protection of XTME-190 type of transducers was achieved by coating the transducer diaphragm with a thin layer of RTV.

Because of the very low pressures attained in this series of experiments, the pressure signals had a high degree of instrument and transducer noise. These were filtered out using a Fast Fourier Transform (FFT) filter. In all experiments, the outputs from HEM-375 and XTME-190 transducers were in good agreement.

Thirty fine-wire, type-S, thermocouples were installed along three principle axes to track the progression of the flame. Approximate flame shapes and flame speeds could be

deduced from the flame arrival time versus thermocouple distance data. Signals from the thermocouples and pressure transducers were amplified, digitized, and stored on a hard drive for analysis and archiving. The amplifiers used for amplifying the signals were located in an adjacent building located ~30 m away from the combustion chamber.

Gas Analysis System

Accurate measurement of hydrogen concentrations in the combustion chamber is important in obtaining reproducible combustion behaviour, which is essential in interpreting the experimental data and in validating codes.

A mass-spectrometer with a 32-channel sampling capability is used to measure the hydrogen, air, and steam concentrations. Using this mass spectrometer the gases in the vessel, including steam, can be analysed to a precision of 0.1% or better. The mass spectrometer requires between 5 and 30 s for the analysis of each sample, depending on the number of gases and the required accuracy. The gas sampling lines are commercially available, electrically trace-heated and insulated, and enable steam concentrations in the combustion chamber to be measured accurately. A steam calibration system has been developed to provide steam for calibrating the mass spectrometer. The mass-spectrometer can detect any gas, provided it is first calibrated with a representative gas mixture.

Experimental Procedure

Safety, Quality Assurance and Documentation

To ensure operator safety and integrity of the facility, a detailed safety analysis report and standard operating procedures were prepared; employing advice from explosives consultants and human factors experts. To guide production of consistent, verifiable output from the facility, a quality assurance manual conforming to ISO Guide-25 documents was also prepared.

Facility Preparation

For vented combustion tests, the required number of steel vent panels are removed and the openings are covered with a thin aluminum foil. To prevent heat losses, the openings are further covered with light insulating panels.

Gas addition to the combustion chamber is performed from the remote control building, following a rigorous procedure to clear the fenced test area. During hydrogen addition, the mixing fans are automatically turned on so that hydrogen released into the combustion chamber mixes uniformly with air. The gas concentrations in the combustion chamber are monitored continuously on the mass spectrometer output during the addition of gases. When the gas addition is complete and the desired gas concentrations are reached, the

mixing fans are operated for a further period of 2 min. The fans are then turned off and the power to the igniter turned on.

Figure 4 shows a typical plot of hydrogen concentration at three different locations. This figure shows that hydrogen concentration in the vessel is uniform during the addition.

In the tests reported here, a TAYCO glow-plug igniter located at the centre of the combustion chamber, operated by a 120-V supply line, was used. The data acquisition system was triggered by a fine-wire thermocouple located in the vicinity of the igniter. For most of the experiments, a sampling frequency of 1 kHz was used.

Results and Discussion

Scope of Investigation

All the tests reported here were performed at an initial temperature of $\sim 27^{\circ}\text{C}$ and at near-atmospheric pressures. Hydrogen concentrations in the range of 8 to 12% were studied. Central ignition was chosen for the initial tests. Three vent areas – 0.56, 1.12 and 2.24 m^2 – were investigated.

Pressure Transients and Peak Pressures

The following section describes results from selected tests illustrating the essential capabilities of the facility, accuracy of measurements and typical test outcomes.

For mixtures containing less than 8.5% hydrogen, the pressure rise due to combustion was small. The flame propagation below 10% hydrogen concentration is non-isotropic and is significantly influenced by buoyancy. Between 8.5 and 9% hydrogen, the flame propagation is first upwards and then downwards [8]. Fig. 5 shows the pressure transient at 8.5% hydrogen concentration. Three pressure peaks can be observed. The first two peaks correspond to the instant of rupture of the foil covering the vents and the instant of burnt gas venting respectively. The third peak corresponds to the final pressure realized by combustion. The peak pressure observed at 8.5% hydrogen is only about 1.5 kPa. This low pressure is expected because, for hydrogen concentrations below 9%, the burn fraction has been shown to be low [8] and the duration of combustion on the order of several seconds, allowing large pressure relief by venting.

Figure 6 shows the pressure transient for a 9% hydrogen mixture and a vent area of 0.56 m^2 . In this case, there are three small peaks and a much larger fourth peak. The small peaks correspond to the vent panel rupture and the burnt gas venting, whereas the large peak corresponds to the final pressure realized by the hydrodynamic instabilities developing in the flame. Similar behaviour was observed for a 10% hydrogen mixture.

For vent areas of 1.12 and 2.24 m^2 and hydrogen concentrations of 11 and 12%, the pressure transients in the vessel exhibited pressure oscillations. Figure 7 shows the

pressure transient registered by one of the transducers for a hydrogen concentration of 11% and a vent area of 1.12 m². The maximum pressure recorded in this series, about 35 kPa, occurred for a hydrogen concentration of 12% and a vent area of 0.56 m².

Effect of Scale and Geometry

It has been pointed out by Solberg et al. [9] that the overpressure in a vented deflagration depends on the scale of the confining vessel. From this point of view, it is interesting to compare the present results with the peak pressures measured in the 2.23-m diameter spherical vessel [10]. Figure 8 shows the peak pressures plotted as a function of the vent parameter, $A_v / V^{2/3}$ (where A_v = vent area, V = vessel volume), for both cases. The peak pressures measured in the 2.23-m diameter vessel (CTF) are higher than those measured in the LSVCTF. Though at first sight this behaviour may appear to be unexpected, it can be explained. It should be noted that the rate of pressure rise and thus the peak pressure in a vessel depends on the rate at which the flame surface area increases with time. Whereas in a spherical vessel, the flame surface area steadily increases with time (for a central ignition), in a vessel of rectangular geometry the flame surface area increases only until the flame touches the side walls. The flame surface area either decreases or remains constant after this. Calculations using VENT [11] show that, other factors being the same, this is indeed what happens.

Flame Speed Calculations

As mentioned previously, fine-wire thermocouples were installed along the three principal axes to track the flame movement. Tracking of the flame front provides a means of arriving at the flame shape as a function of time and of estimating the flame speeds. The instrumentation in this series of experiments provides only approximate flame shapes. A more extensive thermocouple arrangement is intended for future tests.

Figure 9 shows the typical thermocouple traces of two thermocouples installed along the axis, for an 11% hydrogen/air mixture. The flame arrival times can be determined from these traces fairly accurately. There is an abrupt increase in the temperature registered by the thermocouple when a flame contacts it.

Figures 10 and 11 show the flame arrival time plotted as a function of distance for flame fronts propagating in the direction of the vent and in the direction opposite to the vent. A third degree polynomial was fitted through the data points which was then differentiated to yield the flame speeds. The flame speed in the direction of the vent is much higher than that in the opposite direction by about a factor of five. The results are similar for other concentrations and vent areas.

Since the laminar burning velocity of a 11% hydrogen/air mixture is only about 0.3 m/s, the maximum expected flame speed in the direction away from the vent, based on expansion ratio of 4, is about 1.2 m/s. From Fig. 11, this is the initial flame speed calculated from the flame arrival time data. However, beyond 7 s, a short time after the

burnt gas venting starts, the flame speed increases fairly rapidly, reaching about 4.5 m/s towards the end of combustion duration. This flame speed corresponds to a burning velocity of about 1.2 m/s, which is 4 times the laminar value. Such an increase in the burning velocity is possible only through flame front instabilities and wrinkling of the flame.

While the flame propagation can be highly turbulent in one direction, it could still be laminar in other directions. For example, Fig. 12 shows the flame speeds in the upward and sideways directions. In these directions, the flame speeds decrease with time indicating that burning velocities are decreasing. This is plausible because the flame propagates in an adverse velocity gradient. As well, the flame stretching causes burning velocity to decrease.

In the discussions presented above, only a selected few experiments were chosen to demonstrate some of the combustion behaviour. At the time of writing, the facility has been operating for only two months. Further work is required to come to definitive conclusions.

Future Work

The experimental work performed to date has provided knowledge of the duty cycle for operation of the facility and fine-tuning of the instrumentation and measurements. An ambitious experimental program is in place for the coming months. This program will study the effects of elevated initial temperatures, steam dilution, igniter location, initial turbulence, scaling, and flame propagation from one compartment to another. These experiments will provide an understanding of the combustion behaviour in large volumes and a database for validating the combustion models in our containment codes.

Conclusions

A large-scale vented combustion test facility has been constructed, instrumented, and commissioned to perform a variety of combustion experiments relevant to hydrogen behaviour in nuclear containments and meet the needs for codes predicting hydrogen combustion behaviour. Experience in operating the facility indicates that the facility is versatile, easy to operate, and that the gas concentrations in the combustion chamber can be maintained and measured very accurately. Instrumentation and facility modifications can be performed quickly due to easy access to the interior of the combustion chamber.

Acknowledgments

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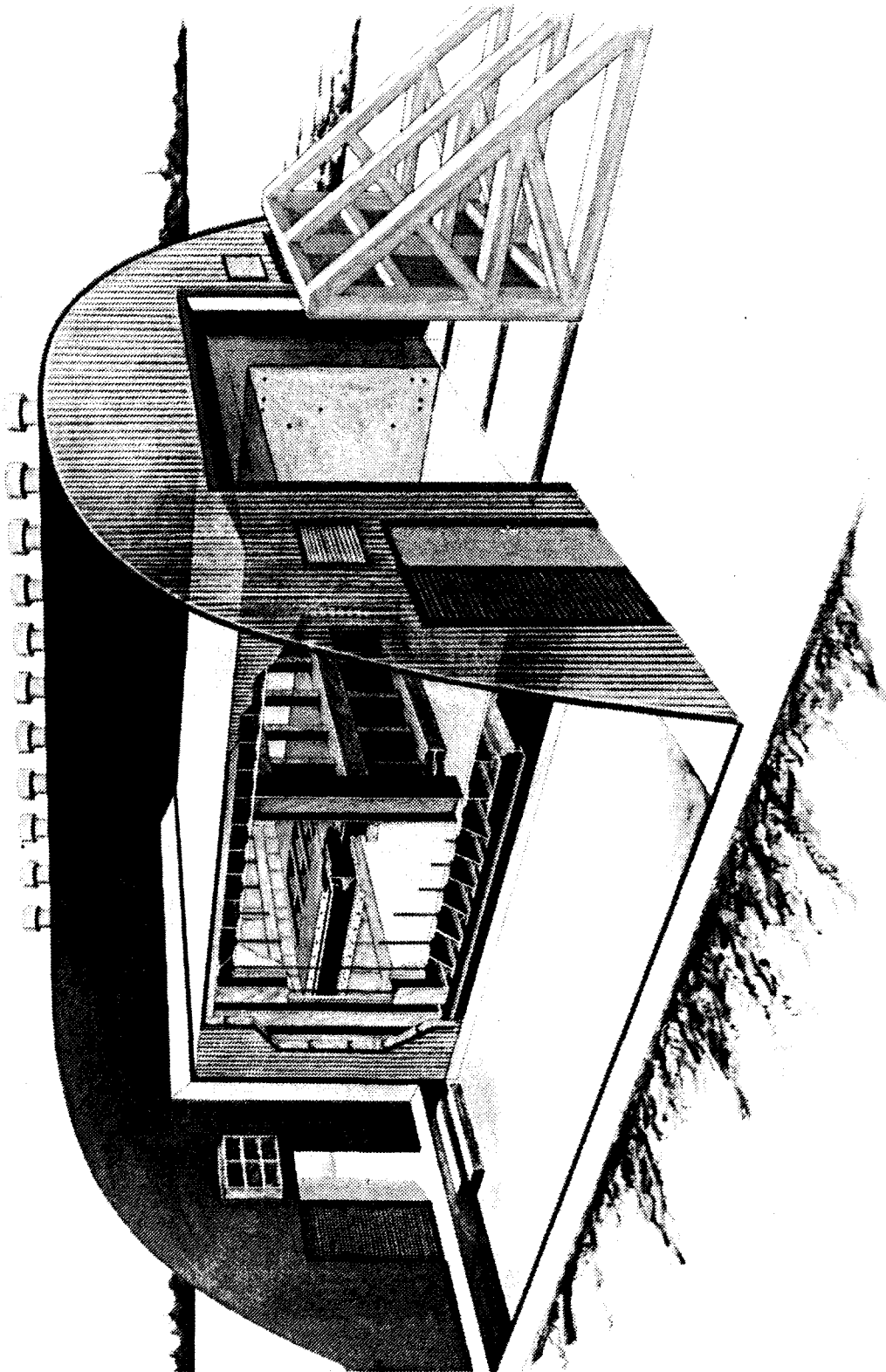


Figure 1: Schematic of the Large-Scale Vented Combustion Test Facility (LSVCTF)

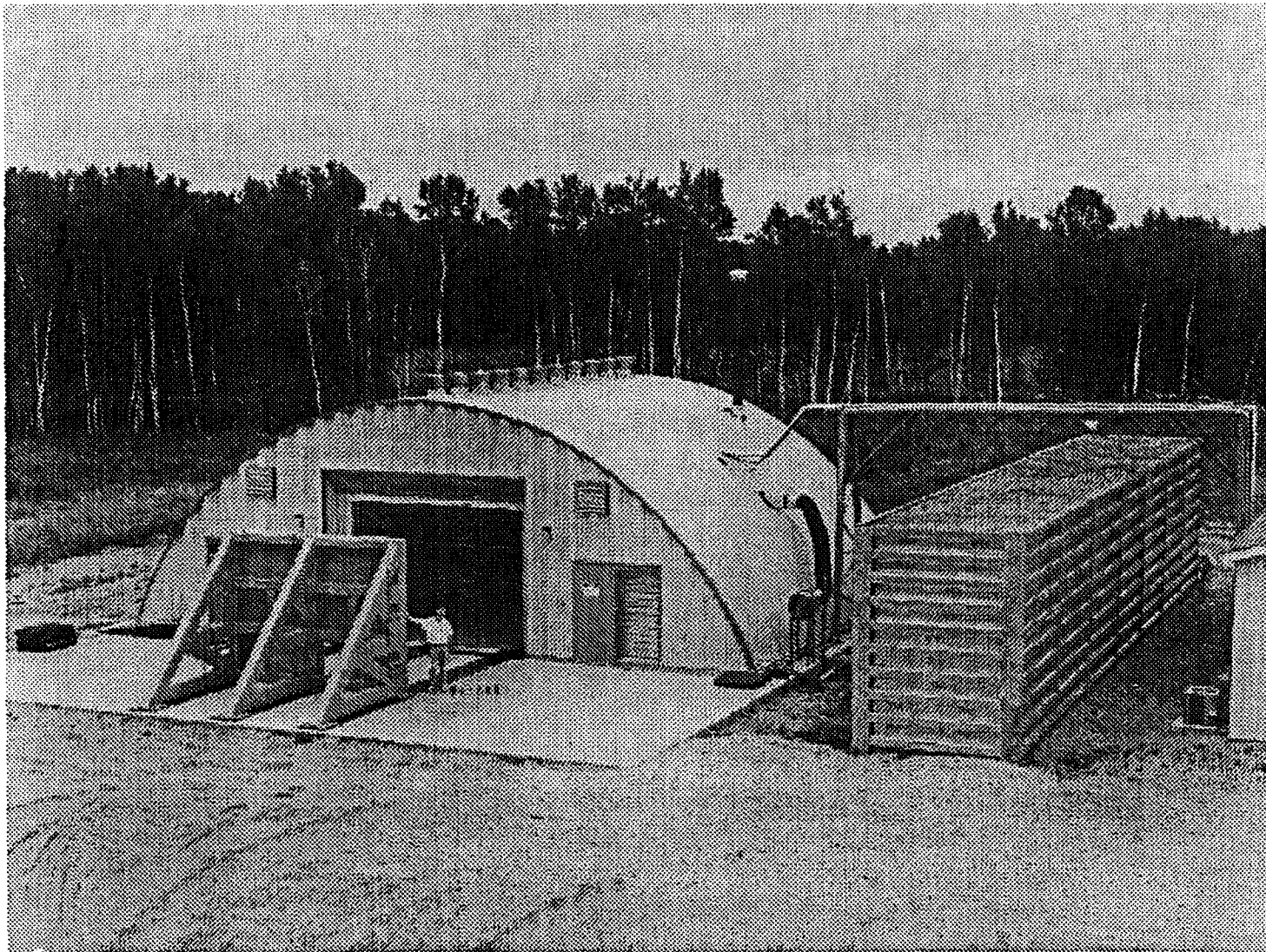
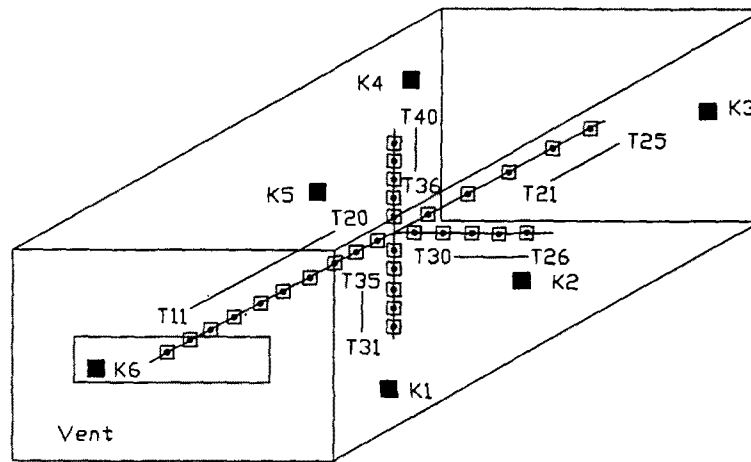


Figure 2. A View of the Large-Scale Vented Combustion Test Facility



T11 to T40--Fine Wire Thermocouples
K1 to K6--Kulite Transducers

Figure 3. Schematic of the LSVCTF Instrumentation

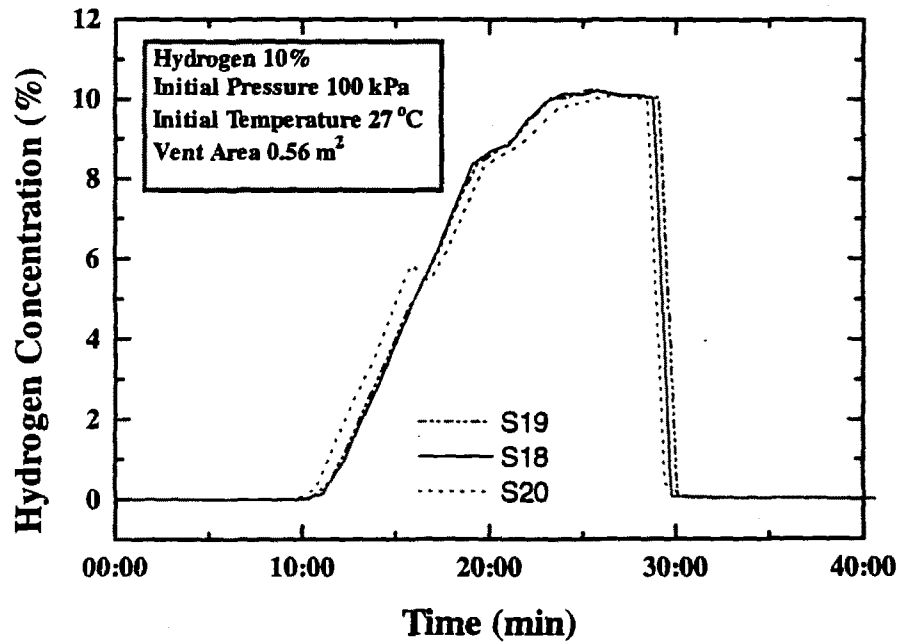


Figure 4. Hydrogen Concentrations at Various Sampling Ports in a Typical Test

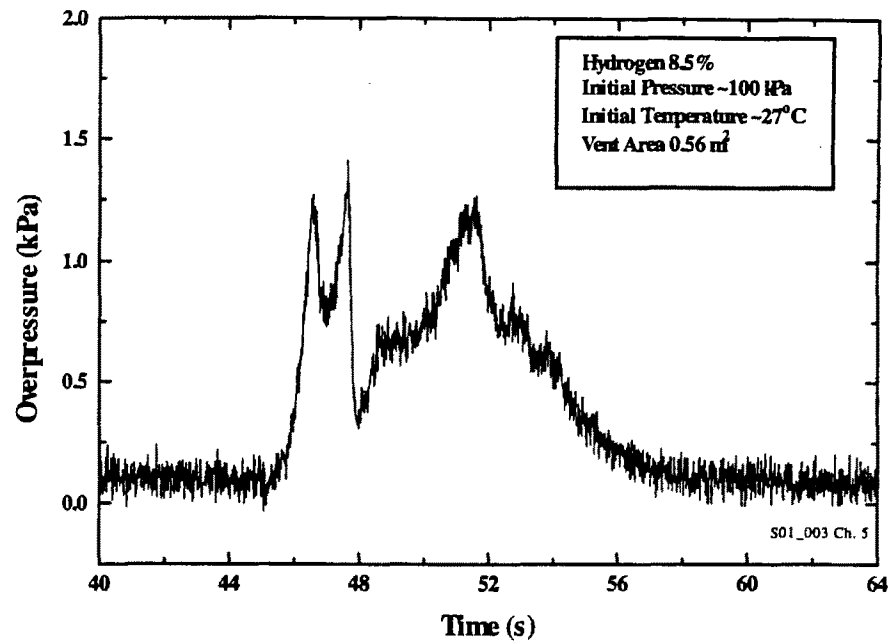


Figure 5. Pressure Transient in an 8.5% Hydrogen/Air Mixture

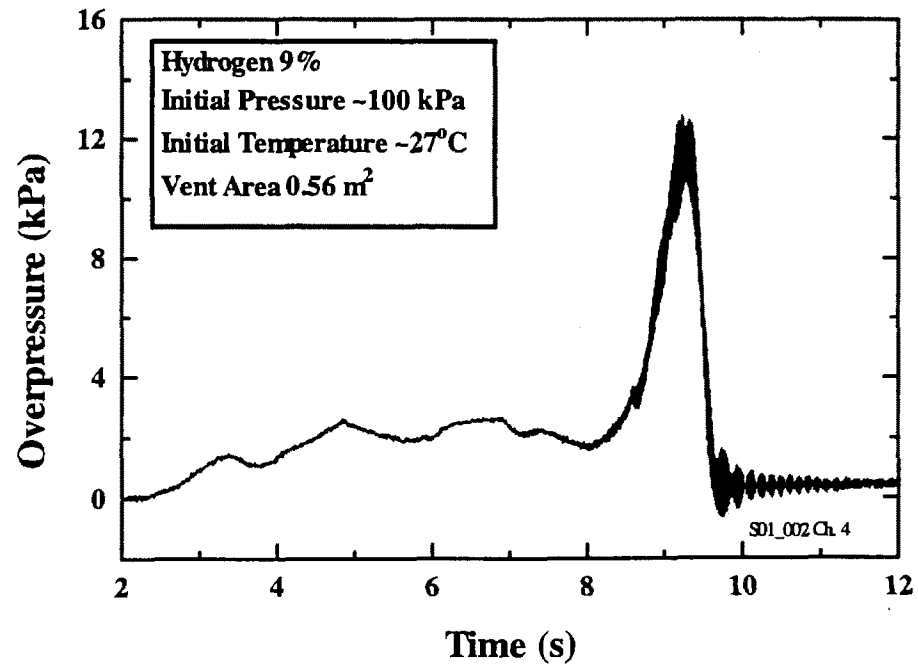


Figure 6. Pressure Transient in a 9% Hydrogen/Air Mixture

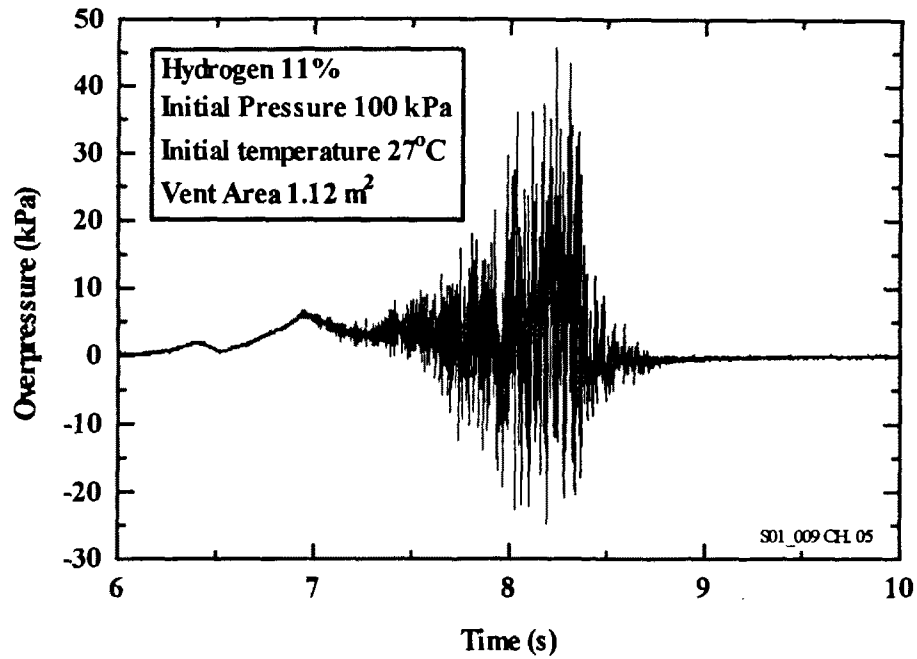


Figure 7. Pressure Transient in an 11% Hydrogen/Air Mixture. Large oscillations occur after the burnt gas venting begins

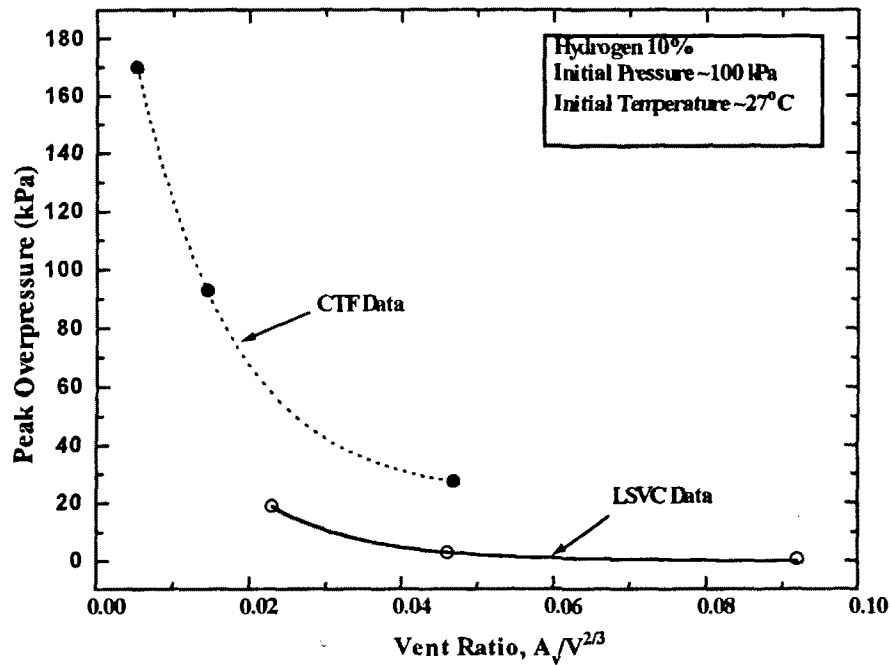


Figure 8. Comparison of CTF and LSVC Peak Overpressures

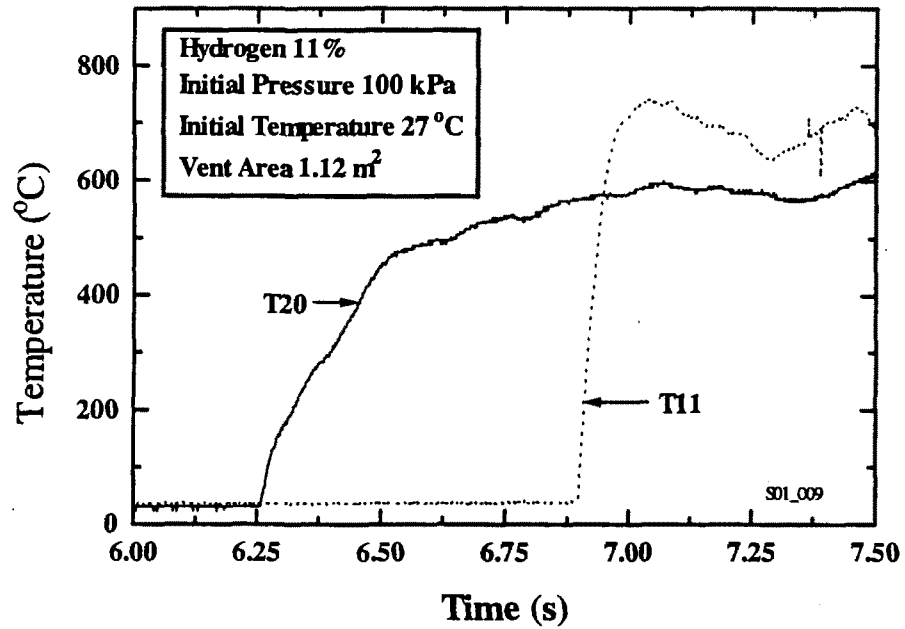


Figure 9. Typical Temperature Traces Recorded by Fine-Wire Thermocouples

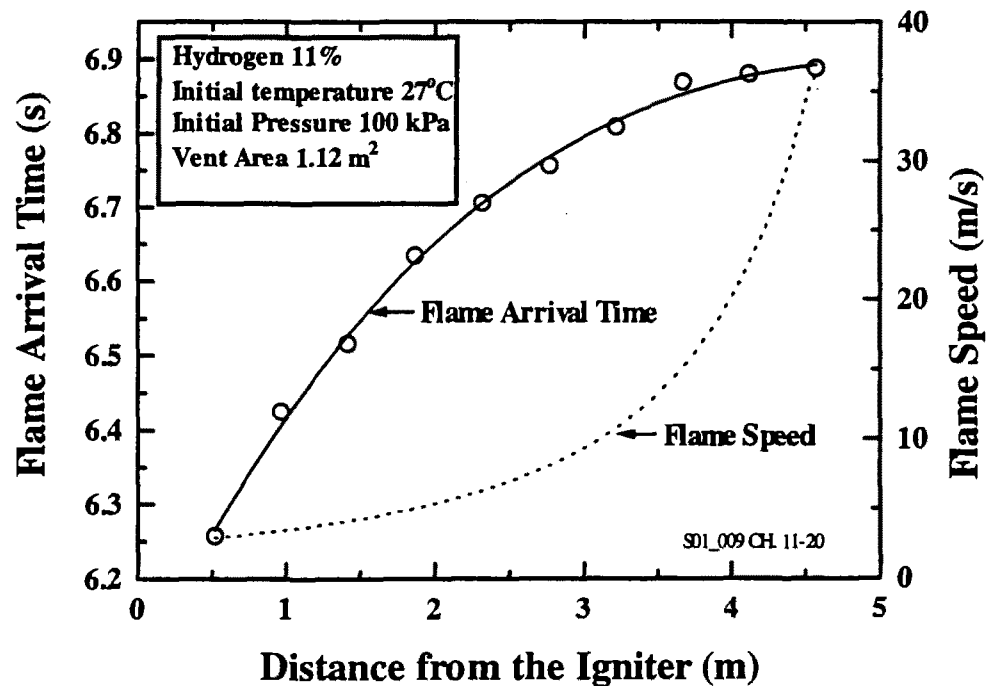


Figure 10. Flame Arrival Time and Flame Speed in the Direction of the Vent

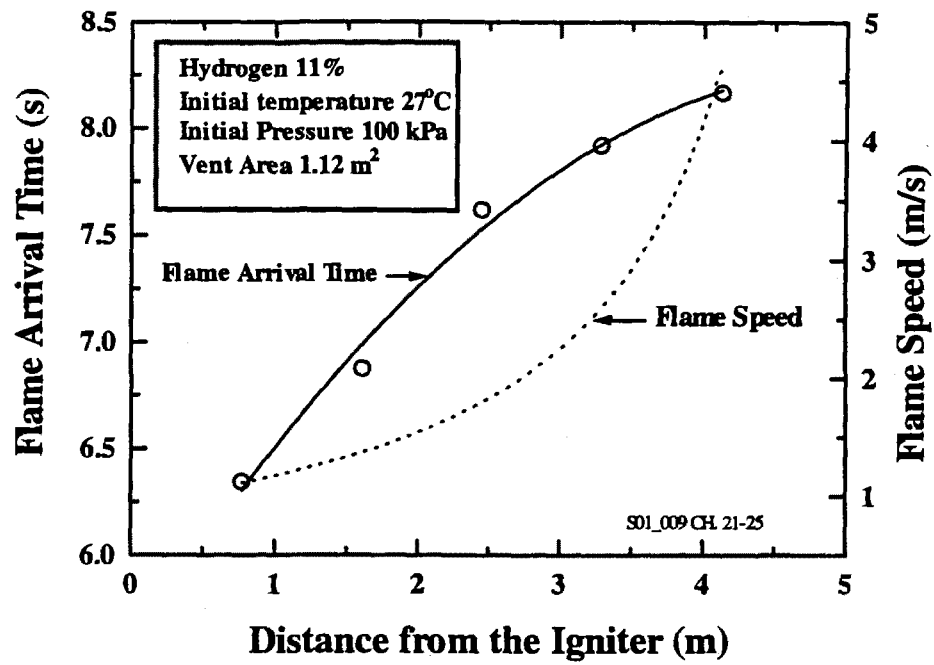


Figure 11. Flame Speeds in the Direction Away from the Vent

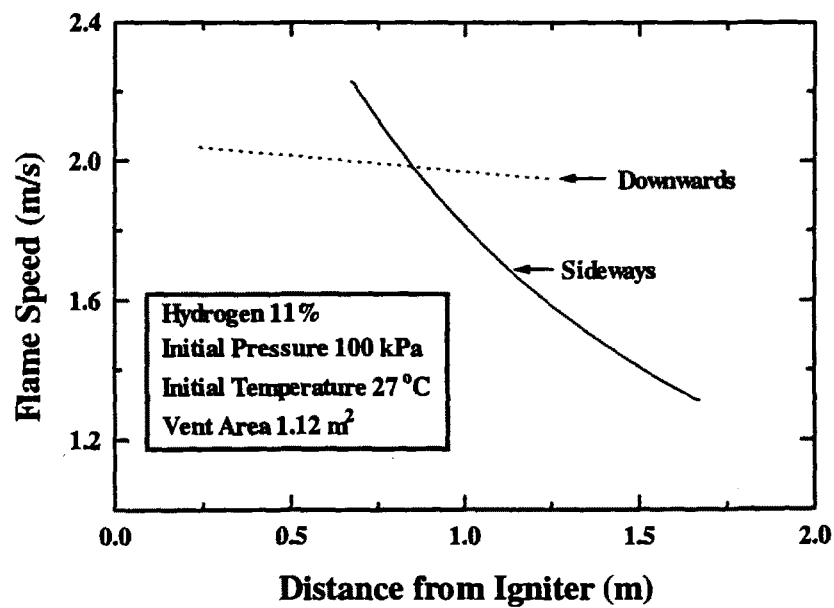


Figure 12. Flame Speeds in the Downward and Sideways Directions

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3.5 DEVELOPMENT AND VALIDATION OF A CATALYTIC RECOMBINER MODEL FOR THE CONTAINMENT CODE RALOC MOD4.0

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CA0000163

Abstract: This paper reports on the development of a catalytic recombiner model for the containment code RALOC MOD4.0 /KLH 95, KLH 96/ and the detailed validation work, carried out at GRS. The model was qualified by using the results of medium and large scale experiments, being performed in Germany /KAN 91/. The comparison of measured data with the calculations demonstrates, that this new model is suitable for real plant applications to investigate the overall effectiveness of a catalytic recombiner system under severe accident conditions for large dry containments of German PWR design. The results of such investigations will serve as the basis to work out some guidance for the determination of the system capacity needed and an optimal positioning of such devices in containments.

This work was sponsored by the German Federal Ministry for Education, Science, Research and Technology (BMBF).

1 Introduction

For the removal of hydrogen, being transferred and accumulated in the containment atmosphere during the course of a severe accident, in Germany it was recommended by the Reactor Safety Commission (RSK) during June 1994 to use catalytic recombiners. Such devices were developed and tested mainly in Germany and Canada. In the last years special models have been developed at GRS to simulate the behaviour of such devices in the containment code RALOC. Especially a box-type of catalytic recombiner was modelled by an one-dimensional junction type using a height depending temperature profile inside the box and the casing. The one-dimensional junction can be divided in several elements. For each gas component in an element the mass and energy equations are solved. The casing and the plate type catalytic foils are simulated by defined structures. For the catalytic reaction rate an Arrhenius type equation is used. The aim of such hydrogen recombiner model is to give reliable predictions for the reaction rate, the temperature of the catalytic foils and the housing, the exit gas temperature and the convective flow rates for different layout of devices.

This model was validated, using medium and large scale experiments, being performed in Germany. The results obtained showed reasonable values, but some weak points still exist, especially to simulate the starting conditions for the catalytic reaction, the influence of low oxygen concentrations in the gasmixtures and the reduced reaction behaviour of the catalysts as a result of surface poisoning. Work on these items are in progress.

In the following, the model will be described, together with the results of selected post-test calculations, performed to demonstrate the ability of this new model simulating the effectiveness of catalytic recombiners.

2 Description of the Catalytic Recombiner Model

2.1 Short description of existing catalytic devices

In Germany two types of catalytic devices have been developed by the industries for the mitigation of hydrogen problem during severe accidents. Both the type of catalytic devices have been qualified by numerous small scale experiments and tested for large scale experiments within the Battelle Model Containment. Although the general feature of the devices are related to common box type, they differ substantially in their selection of catalysts as well as in their individual details.

Fig. 1 illustrates the catalytic device as developed by the manufacturer Siemens. This box type of device consists of thin catalytic plates of stainless steel substrate coated with platinum. The plates are arranged parallelly with intermediate gaps to allow convection flow and combined with the structure of housing a chimney effect is generated.

Other device by the utilities is described in Fig. 2. This concept uses catalysts consisting of Pd-coated alumina granulates stacked within two parallel wire nets forming an element. These elements are arranged parallelly with intermediate channels within a box to form a module. This modular construction is open at top and bottom to develop chimney effect.

A number of these type of devices are foreseen to be placed at various compartments within the containment where substantial release of hydrogen is going to be expected.

2.2 Basic Phenomena, Relevant for a Catalytic Recombiner Model

At first, the essential thermohydraulic processes will be described important for the development of a catalytic recombiner model.

1. Gasdynamics

In a boxtype recombiner flows the hydrogen containing gas mixture through the bottom opening to the catalytic plates. After the initiation of the catalytic reaction, the exothermal heat of reaction will be transmitted to the surrounding gas and a pronounced convection flow will be generated through the box. A fresh hydrogen containing gaseous mixture will thus be sucked into the box. The flow through the box will accelerate until an equilibrium is reached between the heat flux due to reaction and the heat losses to the surrounding gas and to the structure of the housing.

2. Energy Distribution

Heat transfer from the plates to the gaseous atmosphere will be determined initially by natural convection with a laminar boundary layer. With the increasing plate temperature a turbulent convection is generated. As the height of the plates is small (~ 150 mm), so the laminar boundary layer cannot possibly develop fully. Due to this reason, the heat transport will result from combined natural and forced convections.

With the increasing temperature of the plates, the heat transfer due to radiation increases. Radiation heat transfer between the plates is not significant due to slight temperature differences. The absorption of radiation heat by the gas will essentially be determined by the steam content. A part of the heat will be directly transmitted to the surrounding structure of the housing.

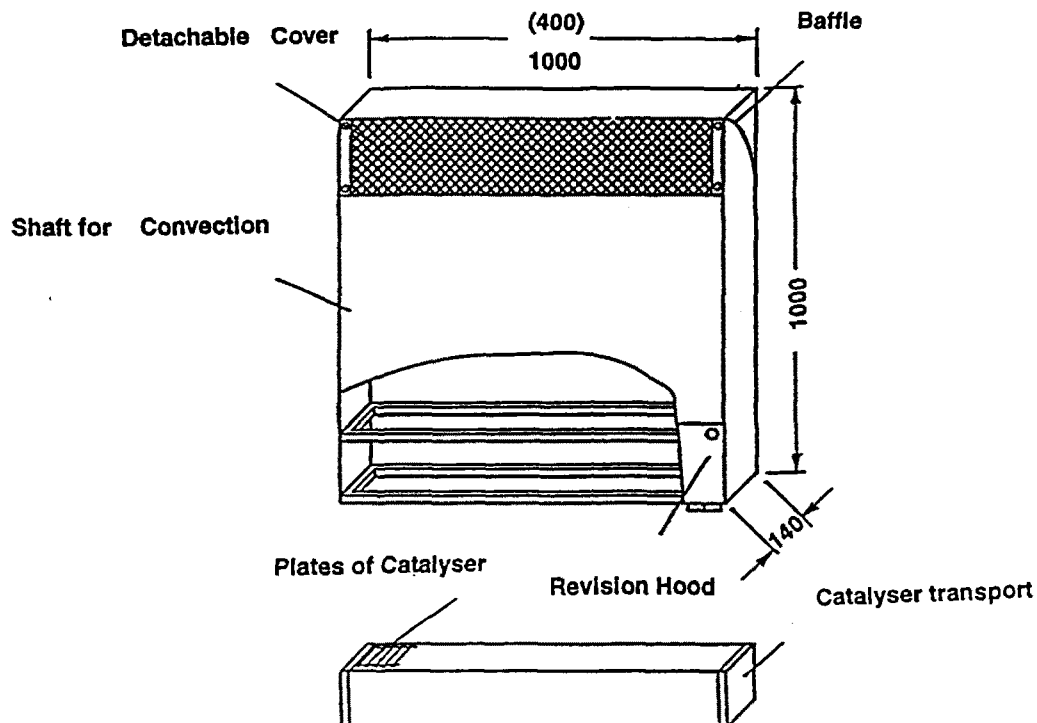


Figure 1 Schematic drawing of the Siemens boxtype catalytic recombiner

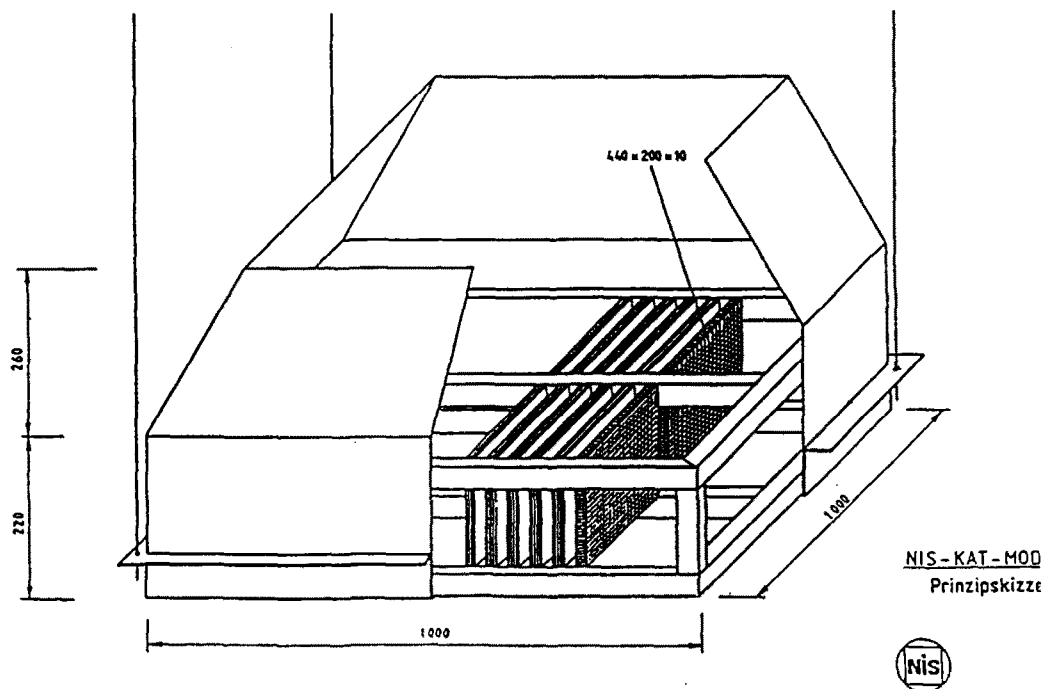


Figure 2 Schematic drawing of the NIS granular bed catalytic recombiner

3. Kinetics of Reaction

It is known that the reaction mechanisms of Palladium and Platinum for the H_2 - O_2 - reaction are similar in principle. However, due to different designs of catalytic recombiner, differences of gasdynamics result causing differences in the rates of recombination. For a boxtype of recombiner, the reaction rate will essentially be determined by the availability of H_2 and O_2 at the catalytic surfaces. Thus the H_2 - and O_2 -concentration reduces along the plates significantly. These sort of influences should be considered for the modelling of reaction behaviour of different recombiners by specific coefficients in the equation, describing the kinetic reaction.

4. Additional Process

- Behaviour of water droplets
Water droplets sucked in the box will evaporate and influence the heat transfer from the plates
- Bypassing
At large distances existing between the plates, the amount of gaseous mixture taking part in the reaction can be limited
- For different plate distances, the view factors for radiation exchanges change. For consideration of such parametric conditions corresponding equations in the model should be realised
- In the boundary layer on the surfaces of catalytic plates temperatures above 600° C can occur. With such high temperatures a thermal recombination can proceed directly in the gas phase generating the heat of reaction directly in the gasflow. Thus a distribution of the reaction heat occurs at the catalytic surface as well as in the gasphase. This phenomenon has to be considered with suitable assumptions in the model.
- The resistance of gasflow through the box consists of individual factors like constriction at the inlet, frictional losses at the plates and their holders and by the expansion at the box outlet. In order to limit the complexities in the model the frictional losses of individual components has been taken into account with a single parameter.

Based on these manifold individual aspects, a simplified model has been developed for the boxtype of recombiner.

2.3 Main Assumption for the Model Development

Especially the model, being developed for a boxtype recombiner is based on the following, simplifying assumptions:

1. One dimensional equations for the balance of mass and energy transport
2. Momentum equation to determine the buoyancy within the box
3. Use of a modified Arrhenius equation for the catalytic reaction kinetics
4. The conditions at the entrance and exit of a boxtype recombiner, like gas concentrations, temperatures, pressure are taken from the lumped parameter code RALOC (connected nodes)

5. The parameters inside the box, like gas mixture composition and temperatures vary only with the height
6. Inside one volume segment, homogeneous mixing is assumed, the conditions are only time dependent
7. Heat transfer between catalytic plates, the structures of the housing and the gas by natural convection and radiation is assumed. Radiation is taken into account for each axial segment separately. The view-factors are calculated for the local geometric conditions
8. Saturated and superheated conditions are considered
9. To stabilize the mass flow rate through the box, the rate is splitted up into two parts, the instationary flow through the box and a stationary part due to the volume expansion inside each element of the flow path through the box.
10. Implicit solution of the set of equations.

2.4 Model Description

Based on the previous assumptions, a detailed model was developed /KLH 96/. With this model it is possible to simulate hydrogen recombination within a boxtype of device. The box can be segmented across the length in multiple segments and thereby leading to a one dimensional junction type between the zones of the RALOC nodalisation.

In this model the catalytic plates as well as the structure of the housing are considered. The heat transfer between the surrounding gas and the structures is calculated by means of free or forced convection, condensation and radiation. Required view factors for radiation will be determined from the geometrical conditions.

The geometrical construction of a box is reproduced in principle in the following figure

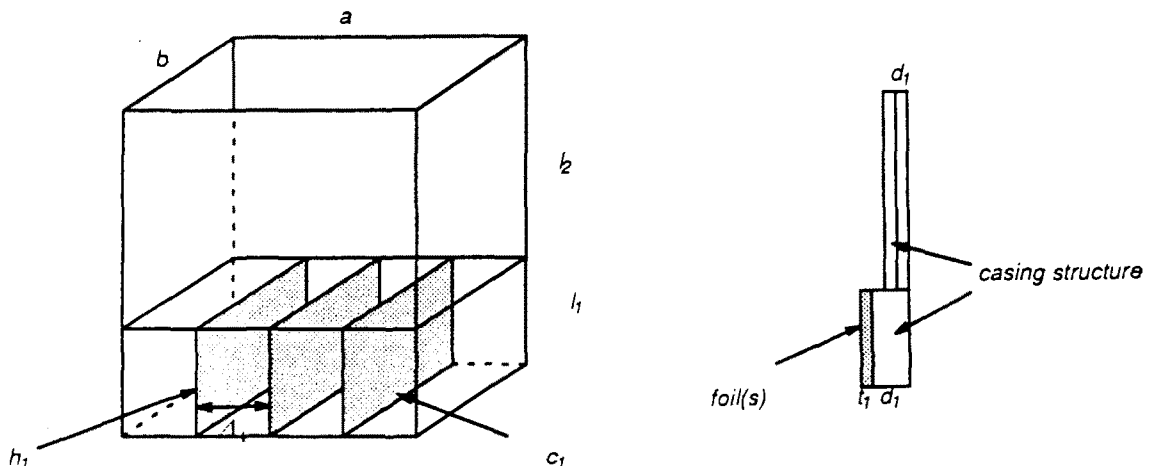


Figure 3 Geometry of the recombiner

The box is subdivided along the height in several segments of volumes with the depth b , the width a and the length l_i . The structure of the housing will be described with the thickness d_i and can be subdivided in several layers. Every segment of volume can contain a given number n_i of catalytic plates with the length h_i , the depth c_i and a thickness of t_i . The distance between the plates is given by Δ_i . The structure of the plates is represented by one layer (for each segment of volume).

With this general description of the geometry numerous different type of structures of recombiners can be simulated.

The equations to calculate the thermohydraulic state of single elements of volume correspond to the equilibrium model used in the code RALOC MOD4.

♦ Equation for Massflow

For stabilising the massflow rates between the volume segments, this will be divided in two parts

$$G_i = G_0 + G_{S_i}$$

with an instationary part G_0 passing through the total junction (box), and with a stationary part G_{S_i} considering the expansion in each volume segment. For the treatment of water droplets within superheated volume segments additional assumptions are needed.

♦ Instationary Massflow G_0

The driving force for the massflow through the box is determined by the density differences within and outside the box. The following figure depicts the relationship of inner volume segments to the surrounding zones. If the lower edge of a volume segment lies below the bottom height of the upper RALOC zone, then it will be connected to the lower zone.

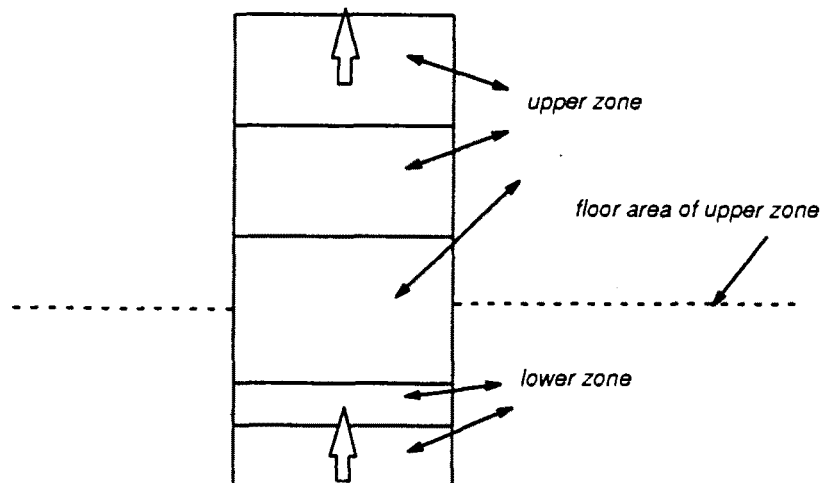


Figure 4 Relation between the volume segments and the surrounding zones

The equation of massflow G_0 through the recombiner is analogous with the instationary atmospheric junction type in RALOC.

♦ Stationary Massflow Equation for G_{s_i}

Due to the heating of gases and resulting expansion additional pressure differences are generated. This will be compensated by the stationary massflow G_{s_i} analogous to the stationary atmospheric junction type in RALOC.

♦ Modelling of possible water droplets in the gasflow through the recombiner

If the model for recombination is combined with a non-equilibrium zone, then a possible flow of droplets through the box can be treated. As within the box only an equilibrium state is calculated so assumptions for the treatment of droplets in superheated volume segments will be made analogous to the equilibrium zone model in RALOC. A complete evaporation of the droplets causes a strong inhomogeneous behaviour within the box. Therefore a parameter 'time for evaporation' has been introduced to damp down this effect.

♦ Kinetics of reaction

The form of the equation for reaction corresponds with the Arrhenius expression

$$r = a \cdot A \cdot C_{H_2}^{n_1} \cdot C_{O_2}^{n_2} e^{-\frac{\Delta E}{RT}} \left[\frac{\text{mol}}{\text{s}} \right]$$

where a Reaction constant

A Total surface of the catalytic plates [cm^2]

C_{H_2} Hydrogen concentration $\left[\frac{\text{mol}}{\text{cm}^3} \right]$

C_{O_2} Oxygen concentration $\left[\frac{\text{mol}}{\text{cm}^3} \right]$

n_1, n_2 Constants

$\Delta E(T)$ Activation energy as a function of catalytic temperature $\left[\frac{\text{J}}{\text{kmol}} \right]$

T Catalyst temperature

As the catalytic surface is large compared to the surrounding volume segment so also the constants n_1 and n_2 are large even for low concentrations. Along the plates the change in concentration will be considered by logarithmic interpolation. The thermal energy will be completely transmitted to the plates.

♦ Description of the structures of recombiners

Calculation of heat conduction is analogous to the HECU-model in RALOC /KLH 96/ for the usual structures. In the case of existing catalytic plates in one of the volume segment then this will be described by the first layer of the structural segment. The structures are basically subdivided in equidistant layers.

♦ Calculation of Heat Transfer within Box

Following heat transfers will be considered in the recombiner

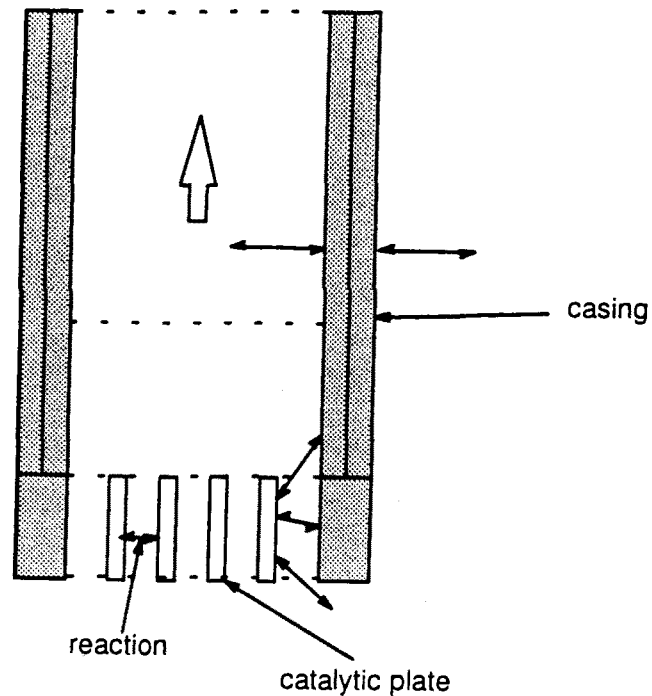


Figure 5 Various Heat Transfer Structures

Convection and Condensation

Heat transfer will be calculated by free or forced convection and by the condensation of the internal as well as from the external side of the housing and at the catalytic plates. The condensate will be transferred to the lower surrounding zone.

Radiation

- Interaction of the external side of the housing with the surrounding atmosphere
- In case of non-existence of catalytic plates
 - Interaction of the interior side of the housing with the volume segment
- In case of the existence of catalytic plates
 - Interaction between the plates and the interior side of the housing considering the gases in the volume segment
 - Interaction upwards with the interior side of housing, respectively with the higher zone considering the gases in the volume segment
 - Interactions downwards with the interior side of housing, respectively with the lower zone considering the gases in the volume segment

♦ Switching Criteria

Criteria for changing from superheated to saturated states correspond to the equilibrium zone models in RALOC. Values of hydrogen concentration for the activation and deactivation of the recombiners will be given by the user. The values for the oxygen concentrations needed are fixed in the program.

3 Validation of the catalytic recombiner model

3.1 Siemens plate type recombiner (REKO-box)

To check the capabilities of the catalytic recombiner model, described above, it has been validated against a number of experiments performed in Germany /KAN 91, MOV 94/. The results of small and large scale recombiner tests performed at the Siemens VB-1200 facility listed in the table 1 and at the Battelle Model Containment facility listed in table 2 were used. All these experiments were carried out with a prototype version of the Siemens recombiner FR90-10, containing 10 catalytic plates.

In the Siemens experiments performed in a vessel of 12 m³ the hydrogen has been injected in a short time period at the beginning of each experiment with different initial conditions. The total pressure, average H₂ concentration in the bulk, the bulk temperature and outlet gas temperature were measured. Despite the small size, leading to a greater influence of the outer surface of the vessel, and the limited instrumentation, the results are useful for the validation.

More detailed experiments have been performed at the Model Containment/Battelle. For these experiments the four inner banana shaped rooms (R5, R7, R6 and R8) and the inner cylindrical compartment R1/3 have been used (Fig. 6).

In the following the calculations performed for the Gx4 and Gx6 experiment will be described briefly.

After a heatup phase with sump valves open to the environment to realize the specified initial conditions, the valves were closed and hydrogen was injected from the bottom into the compartment R5, just below the position of the recombiner. The amount of injected steam was controlled, to ensure an equilibrium state between condensation at the walls and injection leading to a total pressure of about 1 bar. This pressure level avoids an uncontrolled leakage of hydrogen out of the containment system. Due to different steam injection rates into the upper compartment (R5) and the lower compartments (R6 and R8) a stable stratified atmosphere has been realized. Therefore the hydrogen was accumulated mainly in the upper region. A special characteristic of the Siemens recombinator is the threshold value for the start of the catalytic reaction. In the selected tests the recombination started at about 3.2 Vol.-% of H₂. Before the start of a second test-phase 7 h later the sump valves were opened again and the compartments were vented by air. After a second heatup phase to perform stratified conditions, the hydrogen was injected into the lower part of the test facility far away from the position of the recombiner. Because the recombiner was still at elevated temperatures resulting from the previous test, no threshold value to start the recombination was observed. During this phase an unintended air injection took place, followed by a repeated opening and closing of the sump valves to keep the pressure constant at 1 bar. In the late phase (after the steam injection phase) the sump valves were opened at about 10.6 h to avoid subatmospheric conditions.

The distribution of the steam injected into the different compartments led to counter current flow conditions in the openings between the compartments. The experimental results demonstrate an effect of stratification inside the different compartments of the test facility. To simulate this complex behaviour, a very detailed nodalization scheme has been chosen, shown in figure 7. The upper compartments have been subdivided into 4 levels and the lower compartments into 6 levels. For each banana shaped room five columns of zones were used. Especially all connections were subdivided, to allow counter current flow conditions. The inner cylindrical room had been split up into 4 zones at each elevation. Additionally figure 7 includes the different positions of the instrumentation to allow comparison with calculated results.

Figures 7 to 12 show the calculated results for the Gx4 experiment. The temperature stratification is well predicted. Even the temperature oscillations due to the repeated opening and closing of the sump valves was simulated correctly. The same behaviour has been obtained for the hydrogen concentration, reaction rates and the gas velocity through the box. Concerning the temperatures at the catalytic plates some additional remarks are necessary. It is known that the catalytic reaction starts at the lower end of the plates nearby the flow entrance. Therefore strong height dependent conditions exist in the junction segment containing the catalytic plates. This effect could not be simulated, because only one node is assumed in the plate region. The resulting temperatures show that the calculated plate temperature corresponds well to the measured one at the lower end of plate and the calculated gas temperature around the catalytic plate corresponds to the measured upper plate temperature. This leads to the conclusion that the upper plate temperature is close to the temperature of the surrounding gas. In figure 8 the measured reaction rate is given compared with the one being calculated from the overall hydrogen balance in the system.

In the experiment Gx6 the hydrogen has been injected into the compartment R8 leading to mixed atmospheric conditions. The hydrogen flows upward to the recombiner. At a concentration level of about 3.2 Vol.-% the recombination starts. For the calculation the same parameters and nodalisation have been used as for the previous test Gx4. The only exception is the threshold mol concentration used for the starting of the recombination, which differs from experiment to experiment. The physical reason for this variation is not known up to now. The results shown in the figures 13 to 17 demonstrate a similar quality of the results like those of the Gx4 experiment.

3.2 NIS gravel bed recombiner

With the general concept of the recombiner model in RALOC MOD4 it is possible to simulate different types of catalysers, using different values for the reaction constants, loss coefficients and so on. For the validation of the model the MC1 experiment performed at the Battelle Model Containment facility has been used. The principal idea of this experiment is similar to the Gx4 experiment, but with mixed atmospheric conditions. The quality of the calculated results are as good as those gained for the Siemens type of recombiner. The NIS type of recombiner needs relatively long time to get the catalytic reaction started. There is some delay to reach a higher temperature in the gravel bed due to the short height and the high mass of catalytic material.

4 Summary

Models for the simulation of plate and boxtype of recombiners are presently available in RA-LOC MOD4. The validation based on experimental results shows that the essential physical phenomena like temperature behaviour, reaction rates and gas flows for different types can be reproduced fairly well. The degree of nodalisation of the direct surrounding of the recombiners has no direct influences on the characteristic of the models under the same thermohydraulic parametric conditions. The overall structure of the models is flexible enough to simulate various types of recombiners. One dimensional model for the boxtype of recombiners (Siemens and NIS recombiners) has been successfully applied for the postcalculation of experiments. The stability of the models is sufficient enough for the application of detailed nodalisation with a system of recombiners within a real plant under a tolerable computing time.

It can, however, be confirmed that for some single effects, sufficient experimental databasis are lacking presently for further development and realisation of models.

This is specially true for cases like

- Threshold value for the onset of catalytic recombination under various initial conditions (present range: $0,7 \cdot 10^{-3}$ - $1.116 \cdot 10^{-3}$ Kmol/m³)
- Catalytic recombination of carbon monoxide under the simultaneous presence of hydrogen in gaseous mixture
- Upper limiting value of reaction rate and temperature of the catalysts for very high hydrogen concentrations. Clarification of the question, under which conditions an ignition of the gaseous mixture within a recombiner or at the outer wall of the housing can occur.
- Consequence of oxygen deficiency in the gaseous mixture on the efficiency of recombiners

However, for calculations to estimate the overall effectiveness of a system of catalytic recombiners for hydrogen removal during a severe accident in real containment geometries, the achieved status of development of models as well as for validation can be considered as sufficient.

5 Literature

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- /KLH 95/ Klein-Heßling, W.
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GRS-A-2308, Oktober 1995
- /KLH 96/ Klein-Heßling, W., Arndt, S.
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in preparation
- /MOV 94/ Dr. Movahed
Recombiner Model for Lumped Parameter Codes Validation and Application
International Workshop on Hydrogen Research for Reactor Safety, Munich,
19-21 September 1994

Experiment	Siemens Recombiner Test						
	2.2a	2.2b	2.2c	2.2d	2.2e	2.2f	2.2g
Initial conditions							
- Pressure [bar]	1.0	2.2	2.2	2.94	3.76	4.09	4.23
- Temperature [°C]	28	35	97	112	125	132	131
- Peak H ₂ -concentration [Vol.-%]	4.1	3.9	3.9	4.3	3.8	9.1	14.1
- Steam concentration [Vol.-%]	4	2	41	51	61	60	59
Duration of the test [min]	60	80	80	80	80	200	250
Max. deviation between experiment and calculation							
- H ₂ -concentration [Vol.-%]	< 0.2	0.5	0.5	0.4	0.3	0.7	1.8
- Total pressure [bar]	0.01	0.05	0.05	0.05	0.1	0.1	(0.5) ¹
- Bulk temperature [K]	< 5	< 10	10	10	15	15	15
- Outlet temperature [K]	(200)	25	50	(200)	30	(100)	50

Table 1:
Selected Siemens recombiner tests, compared with some calculated results
(experimental results in brackets are uncertain)

¹ Deviation at the end of experiment

Experiment	Battelle Gx-Experiments			
	Gx4	Gx5	Gx6	Gx7.1
Total volume	240 m ³			
Configuration	Network of five compartments			
Boundary conditions during the test:	~ 1 bar in most cases saturated conditions			
- Pressure				
- Humidity				
- Temperature [°C] range				
upper region	~ 65	~ 85	~ 70	~ 85
lower region	~ 40-60	~ 50-70	~ 70	~ 60-65
- Peak H ₂ -concentration [Vol.-%]				
upper region	3.8 / 3.0	3.8 / 2.0	3.2	8.5
lower region	3.3 / 5.5	1.5 / 4.5	3.9	2.0
- Peak temperature of catalyst [°C]	360 / 330	380 / 350	~ 375	~ 560
Experimental realisation	- Steam injection into R5, R6 and R8 (see Fig. 6) - Injection into R5 (near, upper region) + late injection into R8 (far and lower region) - stratified conditions			
			- injection into R8	- injection into R5
			- mixed conditions	- use of igniters
				- stratified conditions
Duration of the test [h]	20	25	25	5,5
Nodalisation used	- detailed nodalisation (143 zones, 276 junctions, 245 structures)			simplified nodalisation (5 zones)
Max. deviation between experiment and calculation				
- H ₂ -concentration [Vol.-%]	0.2 / 1.0	~ 0.5	~ 0.4	~ 1.0 ¹
- Bulk temperature [K]	~ 5	~ 5	~ 10	~ 5
- Plate temperature [K]	~ 20 / 50	~ 20 / 50	~ 25	~ 40

Table 2:
Battelle Gx tests used for validation and comparison with some calculated results

¹ first part of the test

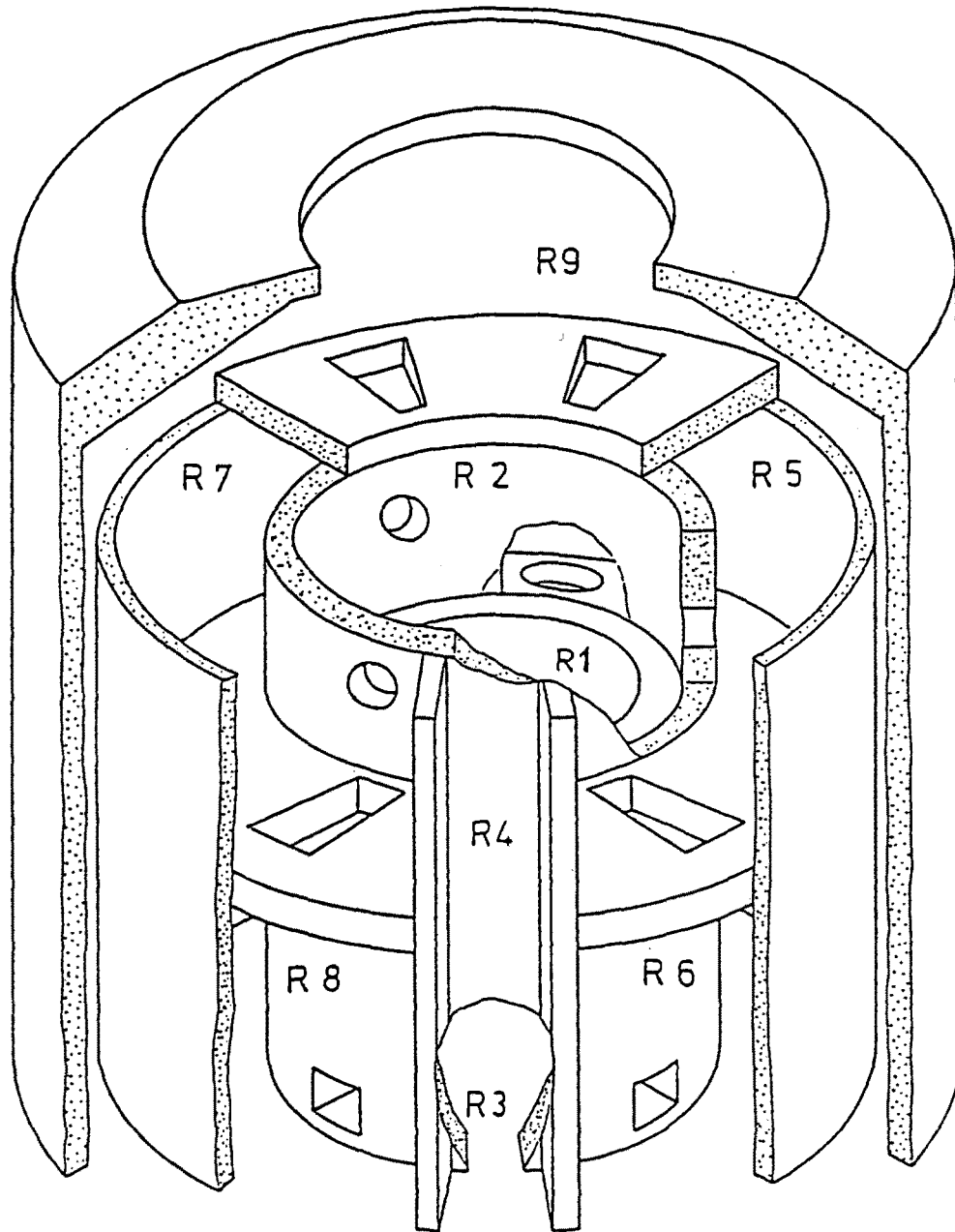


Figure 6 Battelle Model Containment (BMC) Test Facility

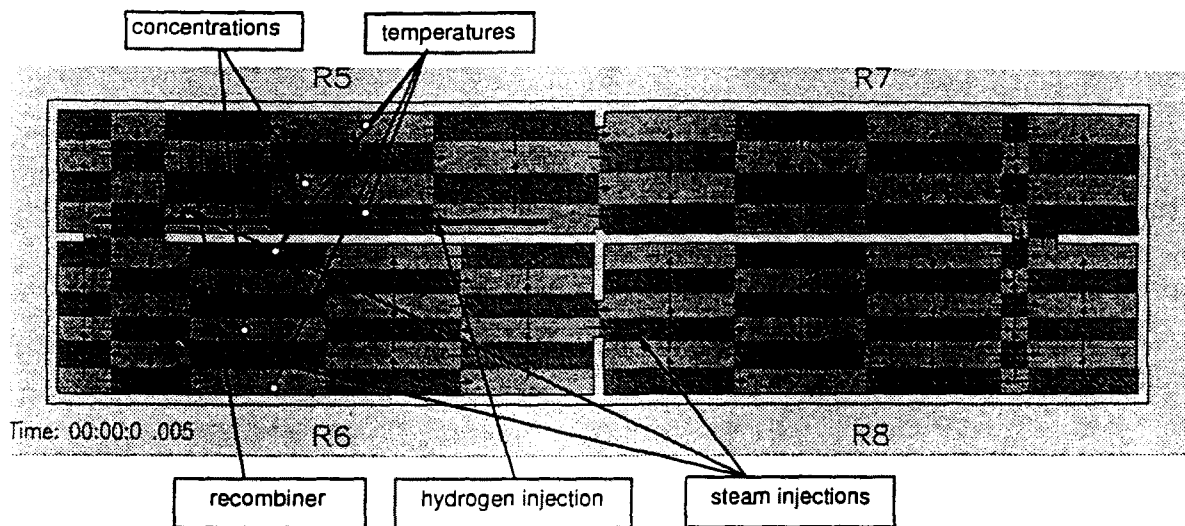


Figure 7 BMC Gx experiments: RALOC-Nodalisation of the banana compartments

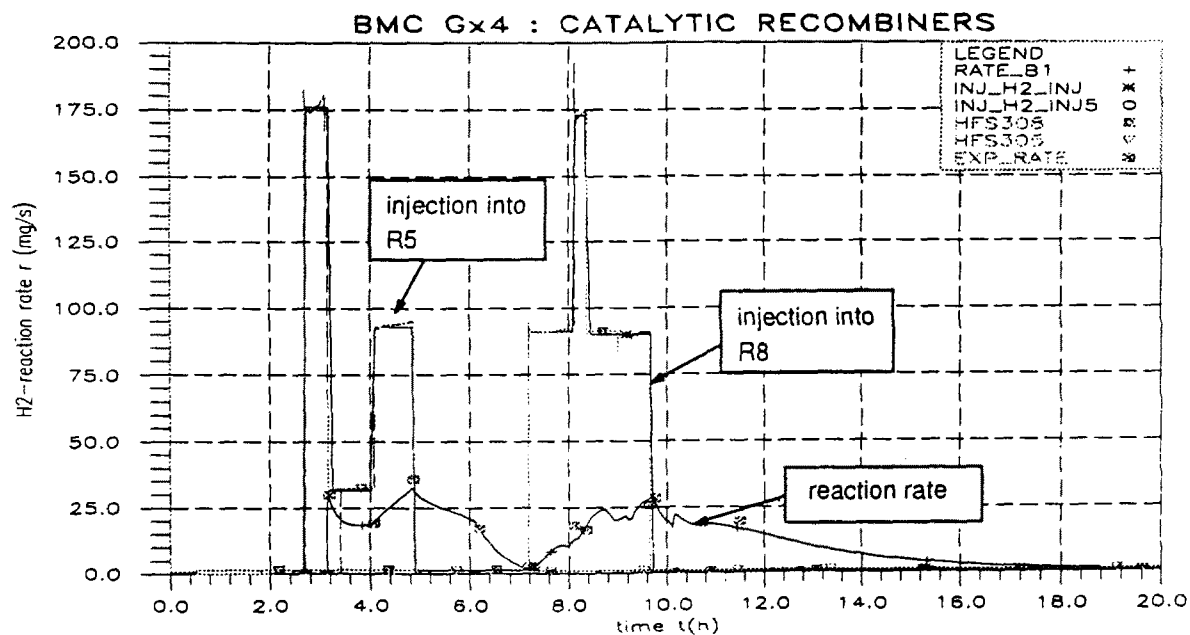


Figure 8 BMC Gx4 experiment: H_2 -injection and reaction rates

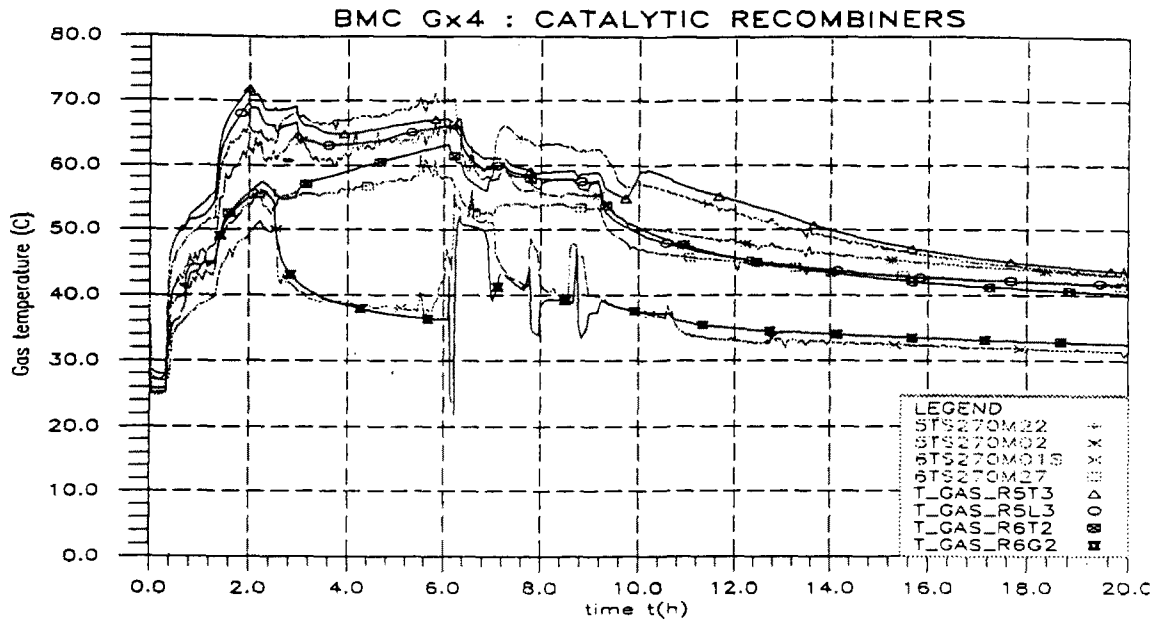


Figure 9 BMC Gx4 experiment: Temperatures in the different compartments

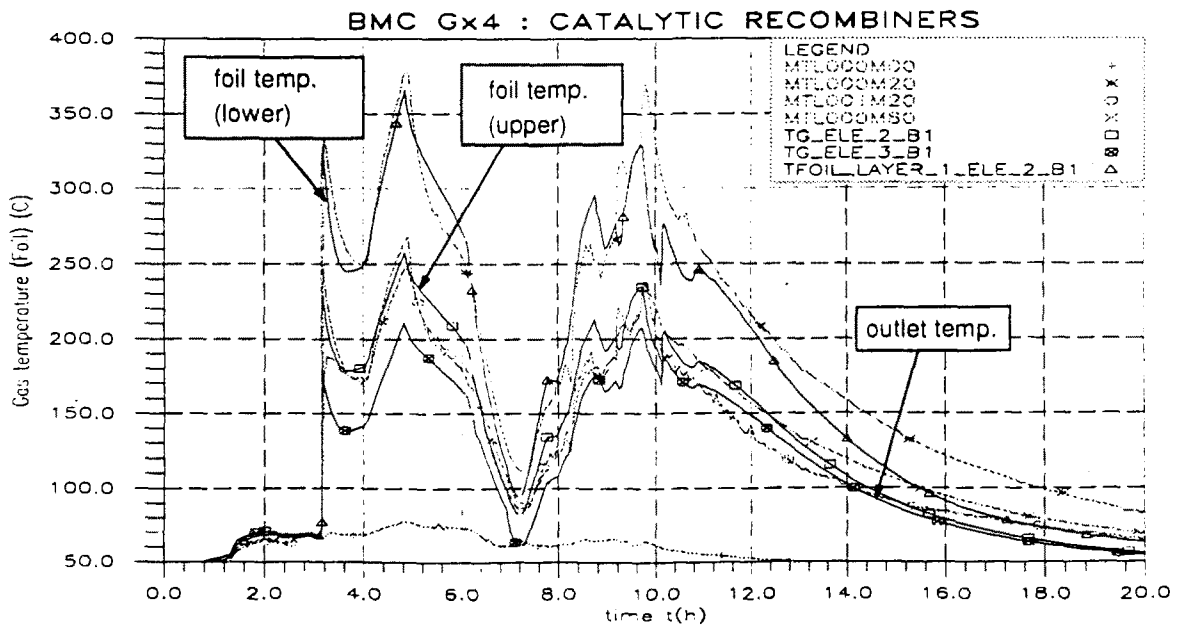


Figure 10 BMC Gx4 experiment: Catalytic plate temperatures

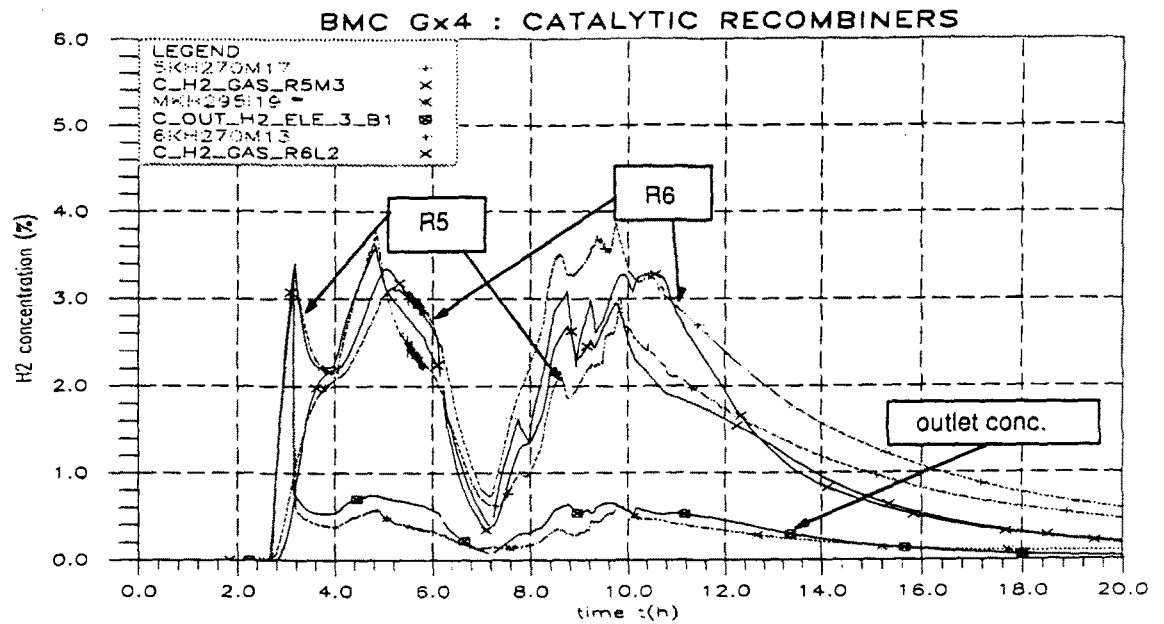


Figure 11 BMC Gx4 experiment: H₂-concentrations in different compartments

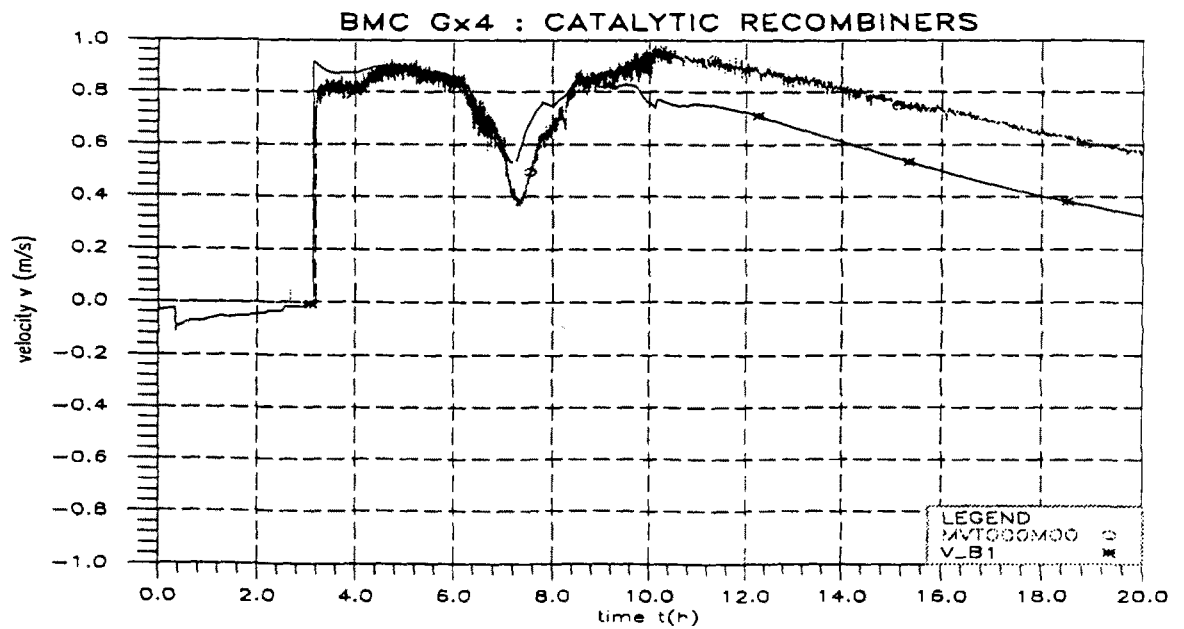


Figure 12 BMC Gx4 experiment: Gas velocity through the recombiner box

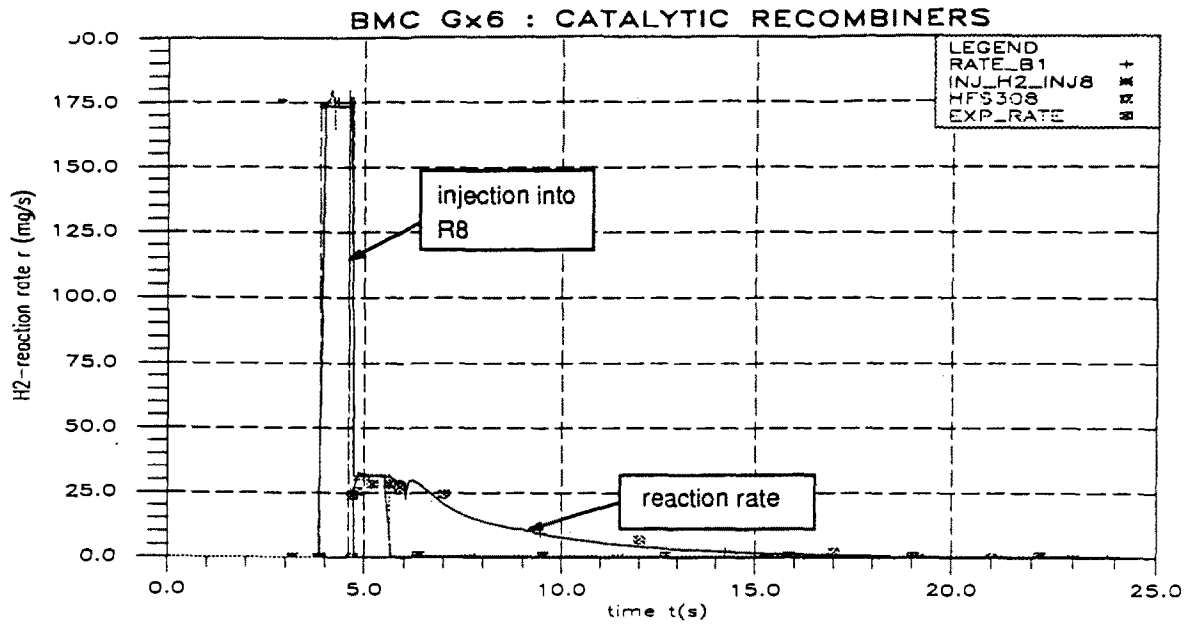


Figure 13 BMC Gx6 experiment: H₂-injection and reaction rates

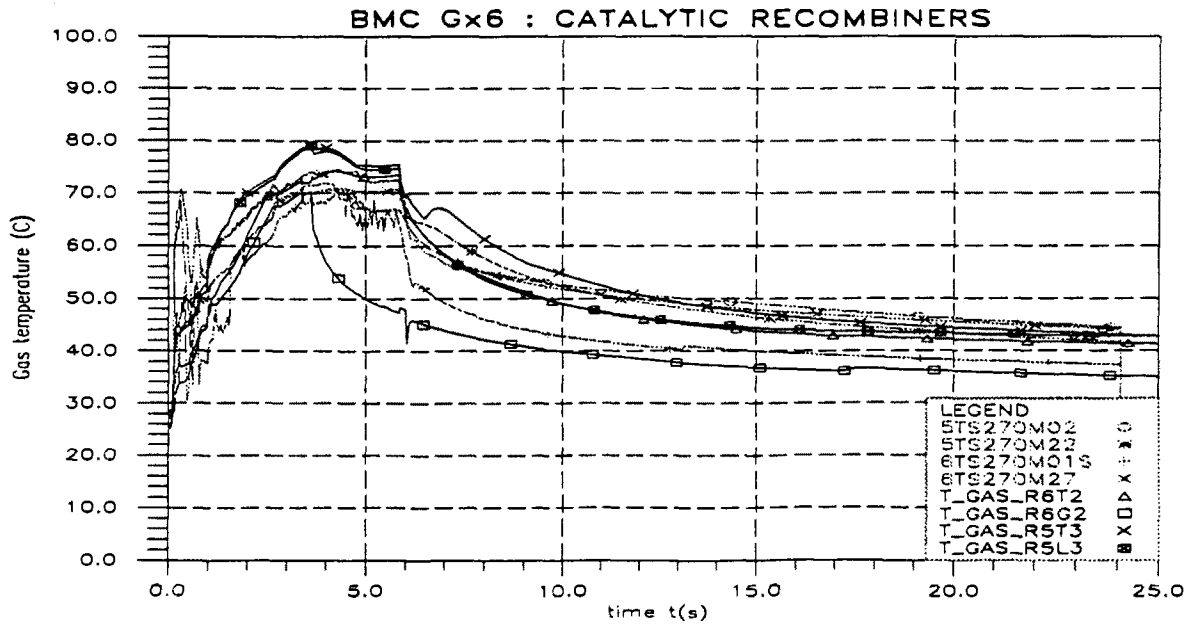


Figure 14 BMC Gx6 experiment: Temperatures in the compartments

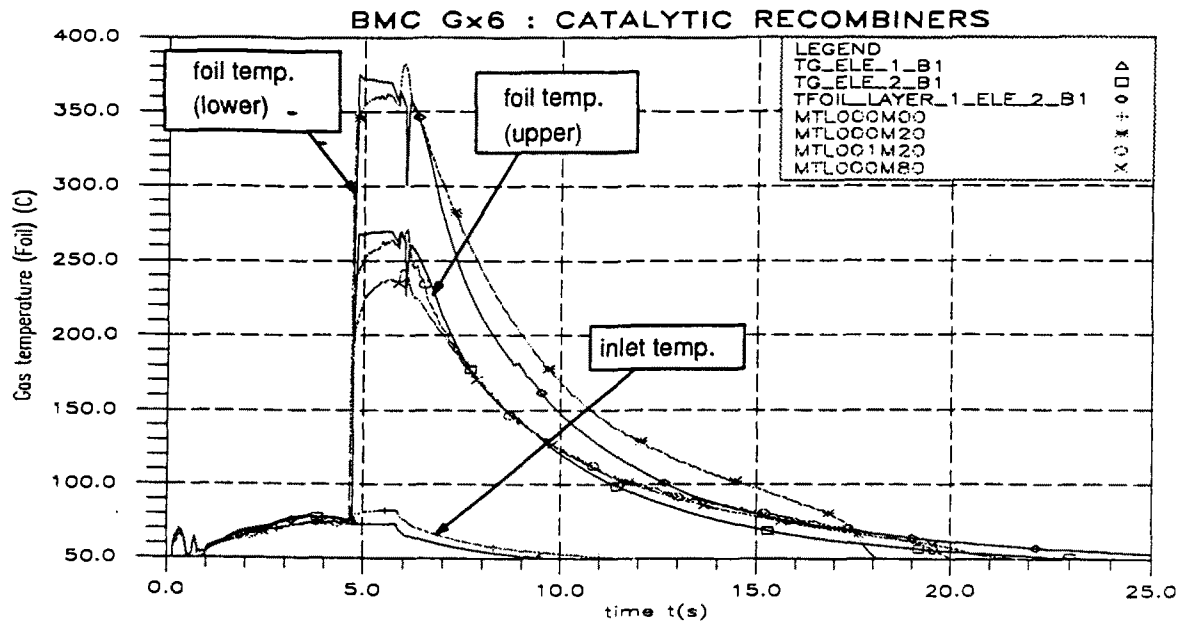


Figure 15 BMC Gx6 experiment: Catalytic plate temperatures

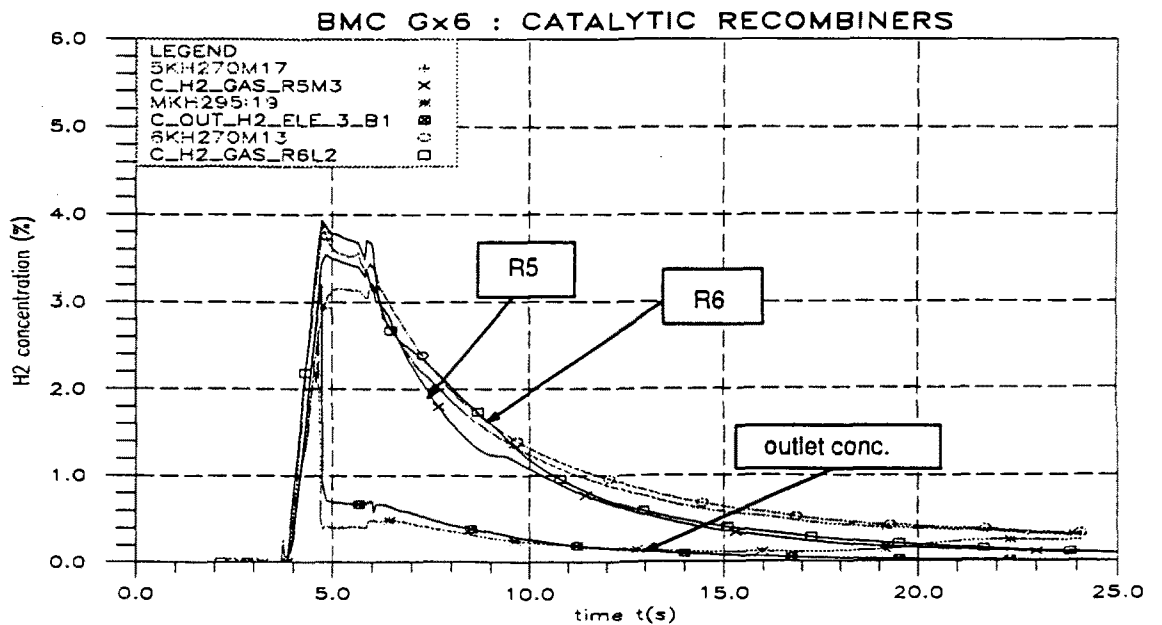


Figure 16 BMC Gx6 experiment: Hydrogen concentrations in different compartments

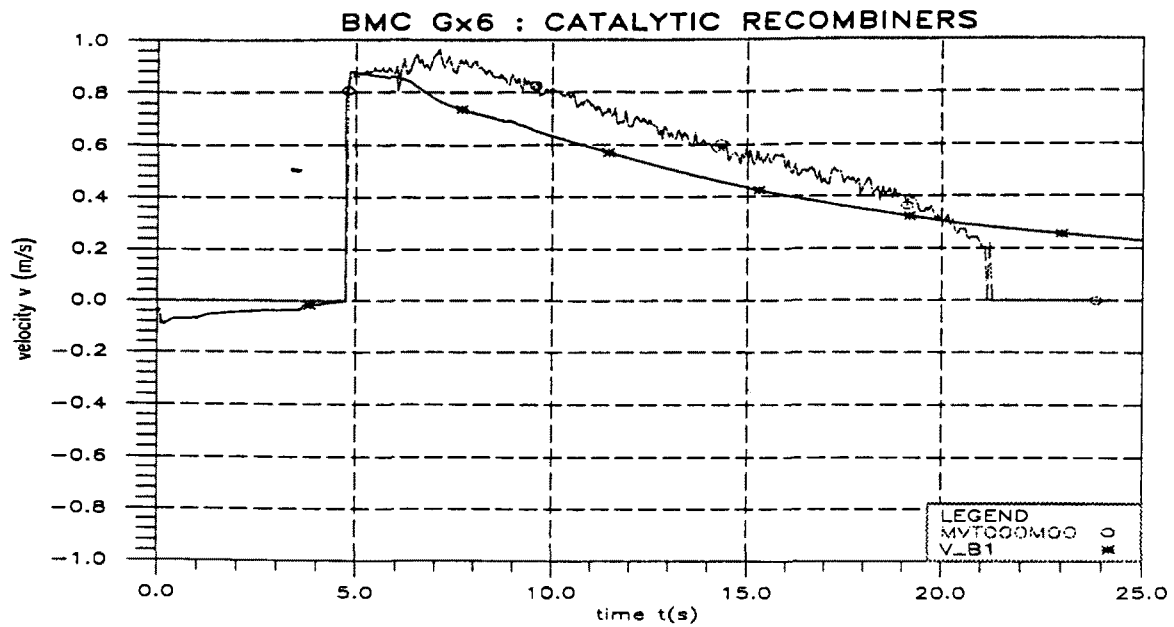


Figure 17 BMC Gx6 experiment: Gas velocity through the recombiner box

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3.6 INVESTIGATIONS FOR THE IMPLEMENTATION OF CATALYTIC RECOMBINERS IN LARGE DRY CONTAINMENTS IN GERMANY

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Abstract: During the past few years, several concepts of mitigation have been developed and tested to limit the hydrogen concentrations in the containment atmosphere during the course of a severe accident. Extensive efforts have been given, especially in Germany and Canada, to investigate the use of catalytic recombiners. Based on the outcome of these research efforts in Germany it was recommended by the Reactor Safety Commission (RSK) in June 1994 to implement a hydrogen mitigation system, based on catalytic recombiners in large dry containments of PWR plants.

Investigations are carried out at GRS, sponsored by the German Ministry of Environment, Nature Conservation and Nuclear Safety (BMU), to develop basic requirements for the implementation of a catalytic recombiner system in large dry containments.

Severe accidents scenarios were calculated with the system code MELCOR to determine the mass- and energy release rates from the primary system into the containment, necessary to prepare the input data for the containment code calculations. A detailed nodalisation of the containment system of a German PWR plant (Konvoi-type) was developed for the code RALOC MOD4 to investigate the effectiveness of a catalytic recombiner system which consists of 53 of such devices, being distributed in the complex room arrangement.

The effectiveness of such a system is demonstrated by comparing a representative severe accident sequence without and with the catalytic recombination of hydrogen. The results showed, that only in some limited areas in the containment combustible gas mixtures were formed for a limited time span and that at the end of the first day after the onset of the accident the catalytic reaction is limited due to oxygen depletion. The work is still in progress while additional severe accident sequences have to be analyzed to develop some generic guidelines for the implementation of a catalytic recombiner system in large dry containments.

1 Introduction

A large amount of hydrogen is expected to be released within a large dry containment of a PWR shortly after the onset of a severe accident, leading to core degradation and melting. According to local hydrogen accumulation, gas mixture composition, turbulence and structural configurations within the containment, the gas mixture can reach the conditions for a deflagration type of combustion or local detonations.

As the containment acts as the last barrier against fission product release, careful planned measures are required to prevent hydrogen concentrations reaching a potential level to threat its integrity.

Extensive efforts have been given in the past years, especially in Germany and Canada, to investigate the use of catalytic recombiners to limit the hydrogen concentrations in the containment atmosphere during the course of a severe accident. Based on the outcome of these research efforts in Germany it was recommended by the Reactor Safety Commission (RSK) in June 1994 (see App. 1) to implement catalytic recombiners in large dry containments of PWR plants.

Within the scope of a project, sponsored by the German Ministry of Environment, Nature Conservation and Nuclear Safety (BMU), GRS carries out detailed investigations to support the ongoing discussions concerning the basic requirements for the implementation of a catalytic recombiner system in large dry containments.

A set of representative severe accident scenarios were calculated with the system code MELCOR. The output of the MELCOR calculations, e.g. gas production rates and fission product heat as a result of core degradation and molten core concrete interaction (MCCI) is required as input for the containment code RALOC MOD4. The following specific questions remain to be addressed now:

- positioning of catalytic recombiners in a multicompartment containment configuration (development of generic criteria)
- determination of the local and overall capacity of a recombiner system, needed to prevent high hydrogen accumulation
- influence of the recombiner system on the gas distribution in the containment under accidental conditions (extent of gas-mixing)
- consequences of a failure of local catalytic devices due to blow-down forces or catalytic poisons.

In addition, when the number and location of catalytic recombiners have been fixed, the overall efficiency of such a system has to be demonstrated by analytical simulation. All these investigations are in progress, using the containment code RALOC MOD4, which contains a verified catalytic recombiner model /ROH 96/. The status and first results of the RALOC-calculations for the German PWR reference plant (Konvoi-type) will be described together with some preliminary conclusions concerning the requirements for the implementation of a catalytic recombiner system in large dry containments.

2 Investigations of Severe Accident Sequences with the Code MELCOR

For detailed investigations about the effectiveness of a system consisting of a number of catalytic recombiners being distributed within a containment to reduce the level of hydrogen being produced during the progress of a severe accident, the data of released masses and energy within the containment are required as input for the calculation with the containment code RALOC. With the actual version of MELCOR 1.8.3 as released by the NRC at the end of 1994, GRS calculates the event sequences in PWR and BWR plants leading to core damages.

The data set of MELCOR reproduces a relatively detailed picture of German Konvoi plants. The applied nodalisation of the primary and secondary circuits is based on the knowledges of thermalhydraulic analyses essentially derived from the detailed code ATHLET and is oriented according to the requirements of integral code analyses. The four loops of a real plant are simulated with 2 loop arrangements including a detailed modeling of the reactor, pressurizer and its relief tank. Systems for plant operation and control are simulated as far as they will influence the accidental sequence. In total ~ 30 thermohydraulic volumes and ~ 50 flow junctions are applied to model the reactor system (see fig. 1).

For a detailed description of the processes of core melting, the reactor core is subdivided in 13 axial layers (10 "active" layers) and in 5 radial rings. The radial subdivision is oriented according to the orientation of the arrangement of fuel elements as well as on radial power distribution.

The containment vessel, the outer annulus as well as the auxiliary building and the environment are modeled with a total of ~ 30 thermohydraulic volumes and ~ 60 flow junctions. This includes the in-service recirculation systems of the containment vessel and the annulus section, the accidental annulus suction system with filters and the filtered containment vent system.

Together with the compilation of data set, the qualification of the nodalisation and code input has been pursued with the following comparative calculations:

- ATHLET-MELCOR: concerning the thermohydraulics of the reactor system till the beginning of core heat up
- RALOC-MELCOR: concerning the situation in the containment
- WECHSL-MELCOR (CORCON): concerning the concrete-core melt interactions including the occurring gas release

The results of these comparative analyses showed in most of the areas good agreements of essential parameters as well as in the general descriptions of the plant behaviour during the accident progression.

With the compiled data set, the event sequences of a total loss of feedwater supply with primary system depressurization (typical ND* case) and a double-ended break of the surpline at the connection to the hot leg (typical ND case) have been analyzed. With these two completely different events concerning the times of onset as well as the progression of core damage were analyzed.

The point of times of characteristic events for the ND*-sequence are listed in table 1. In the following a short description of hydrogen generation and release in the containment for the ND*-sequence will be given as this case has been considered afterwards for the investigation of the effectivity of the catalytic recombiners.

The largest part of H_2 concerning the in-vessel phase of core damage is generated during the time interval of 3.5 - 4 h and is essentially before the last injection phase from the accumulator into the hot leg. The relocation of molten core materials at the lower regions of core with existing water results in the formation of a large amount of steam and consequently the oxidation of metallic surfaces. The heat generated by this exothermic reaction can temporarily reach a multiple value of the amount of decay heat and accelerate the process of core damage. Before the slumping down of molten materials into the lower plenum 400 kg of H_2 and due to the quenching process another 150 kg will be generated. Here a small part of H_2 is also formed by

the oxidation of steel structures of the core region. At the time of the failure of the reactor pressure vessel 45 % of zircaloy inventory has been oxidized.

After vessel failure and with the onset of melt-concrete interaction, the hot gas out of the reactor cavity feeds into the adjacent lower and intermediate compartments of the containment. In the reactor cavity a maximum temperature of the melt will reach 2500 K resulting in an increase of the cavity temperature up to 1600 K. This temperature remains in the same level for 17 h after the onset of the initial event till to a sidewise ingression of sumpwater, following the melt-through of the cavity wall. Due to the resulting strong evaporation, a large amount of hot gases from the reactor cavity will be carried out to the adjacent compartments. Thus the atmospheric temperature in these lower compartments will locally reach a value of about 550 K.

Including the contribution of melt-concrete interaction ~ 2.6 t H₂ and 5.2 t CO will be produced within the first 30 h. Here the ratio of the amounts of H₂ to CO will be measurably determined by the concrete composition.

3 The Containment Code RALOC MOD4

The original objective of the development of the RALOC-code /JAH 86, JAH 88/ was to calculate the Radiolysis and Local Gas Concentrations within the structures of a containment. Further developments /SCH 92, SCH 94/ have been pursued to use the code extensively. In the course of developments and for intended coupling with other codes both the tasks and the structure of the code have been changed fundamentally since 1991. Simultaneously, the models have been verified continuously, supplemented according to technical status and new models added to the code.

The computer code RALOC is able to evaluate:

- pressure- and temperature build-up and history
- local temperature- and pressure distributions
- energy distribution and local heat transfer and heat conduction in structures
- local gas distributions (steam and different non condensable gases)
- hydrogen combustion /HET 95/ and catalytic recombination /ROH 96/
- water distribution
- mass- and volume flow for the release of fluids via opening and leakage
- heat- and combustion gas distribution during fires

Calculations can be performed for simple and multi-compartmented containments and closed buildings of nuclear power plants, as well as for compartmentalized systems (buildings, tunnels, pit system) with more or less large openings to the environment. Mainly the consequences of design basis accidents and severe accidents were analyzed with the code in containments of LWRs i.e. for PWR and BWR, but also in containments of VVER power plants. Some fire events have been investigated, too.

For the description of the physical processes during an accident propagation arbitrary compartment systems and -geometries can be simulated by specified volumes (the so called 'lumped parameter' concept). The conditional changes related to location and time are reduced to a

purely time dependent behaviour within the control volumes (nodes). These volumes are connected by 'junctions'. For the simulation of heat transfer and heat conduction via walls and internal components specified structures can be coupled to the nodes. The heat conduction is described in one dimension, for the simulation of heat transfer processes (heat- and mass transfer) different models and correlations are available.

For a realistic description of accident sequences the simulation of engineered systems is possible like pumps, heat exchangers, ventilation systems, weir, doors and flaps of different kinds with inertia effect, spray systems, catalytic and thermal recombiners and pressure suppression systems.

For the validation of the different models a large amount of experiments have been analysed, as pre- and post-calculations. The code has successfully been used for 4 International Standard Problems and some other benchmark exercises.

4 Data Required for the Simulation of a Large Dry Containment in RALOC

In order to achieve a high degree of predictability, large scale resolution capacity of the input data for the analyses with RALOC are principally required. Especially, this is concerned for the representation of real compartments of the reference plant by the so called model compartments of the code. The high degree of detailing is required for the simulation of long time convection processes in a containment which consequently determines the resulting local H_2 -concentrations and hereby the local positioning of recombiners.

Also a high degree of detailing is required to cover the junction openings between adjacent compartments according to sizes, places, positions and direction. These junction openings can be splitted in free openings, openings resulting from pressure differences like doors, burst flaps, rupture discs as well as in ventilation and drainage junctions.

The exact knowledge of the real containment is required for this. These detailed knowledges are gained from the drawings of the building, supplementary documents of the vendor and utilities as well as from plant inspections. As for example, the rupture discs made of very thin stainless steel and located above the steam generators, are represented exactly by a distribution function of pressure difference of the opening. Such rupture discs create according to ventilation technique a separation between the passable compartments during operation and the non-passable area. Thus, they function as barriers for convection flows in the containment during an accident. The high data quality is also asked for other openings, dependent on the build-up of pressure differences, for which one applies realistic values of pressure differences generating openings as input.

Also, for the free volumes of individual compartments as well as for the heat conducting structures within them, and for the ceiling and walls of the rooms made of concrete, steel and other materials, only the most realistic values are to be applied.

Out of the above mentioned requirements for the preparation of the input-data, the RALOC nodalisation was developed, which consists of 106 compartments for the containment vessel, 14 for the outer annulus section and 9 for the auxiliary building.

Moreover, the input dataset for RALOC contain totally 462 junctions, 226 heat slabs and for the first application a system of 53 recombiners (21 large, 18 medium and 14 small size recombiners, see fig. 2). The nodalisation and the position of some recombiners could be seen from fig. 3.

5 Boundary Conditions, Assumptions and Input Data taken from MELCOR Calculations for the detailed RALOC Investigations

From calculations of accident sequences with the code MELCOR (see chap. 2), input data for the code RALOC on the release of water, steam, hydrogen, carbon monoxide, carbondioxide, system heat and the heat developed by volatile fission products were prepared relating to a specific accident. Here the numerous data of time points resulting from the MELCOR calculations (~ 800) have to be reduced to 20-40 time points for the RALOC input for saving computing time. Apart from harmonization of release rates, efforts should however, be given to comply with the characteristic progress of release as well as to maintain the integral values for the mass and energy input to the containment. For the RALOC investigations of the ND* accident sequence, two calculations have been carried out

- Reference calculations without recombiners (up to 56 h of problem time)
- Calculations with catalytic recombiners (up to 33 h of problem time)

The calculation for this reference sequence was stopped, when the design pressure of the containment was reached. For the RALOC-calculation with catalytic recombination, the diminishing O₂-concentration was the main factor to terminate the calculation. In average the calculating time with an IBM-RISC System/6000 (Model 550) was higher by a factor 20 compared to the actual problem time.

As for the location of release of mass and energy into the containment concerning the ND* accident sequence, the compartment of the pressurizer relief tank within the containment, has been taken (in-vessel phase). After the melt-through of the reactor pressure vessel, the location of release has been changed to the reactor cavity (ex-vessel phase).

The criteria for the arrangement of the recombiners within the individual compartments have been derived from the reference calculations. Primarily, in those compartments where a critical H₂-situation was reached due to the progress of accident (exceeding locally the limits of deflagration up to the limits of detonation), recombiners were arranged as a first conception. The selection of the sizes of recombiners was made according to local availability of space. In the regions where the release of mass and energy will take place for the ND* accident, no recombiners were foreseen.

6 Comparison of Calculated Results without and with a Recombiner System

In the following, some results of the two calculations performed for the ND*-sequence will be described. The two cases, without (reference case) and with a catalytic recombinder system needs at first the hydrogen release rates from the primary system and in the late accident phase from the reactor cavity, which are illustrated in fig. 4 and 5. The high peak release of app. 0.7

kg/s in fig. 4 results from the reactor pressure vessel-failure, while in the longterm ex-vessel phase the mean rate is about 0.02 kg/s. The total mass of hydrogen, being released into the containment atmosphere is given in fig. 6, together with the integral value of the mass of hydrogen, being recombined in the time span of analysis. The actual overall recombination rate is presented in fig. 7, together with the main accident phenomena, occurring during the sequence, being calculated. From fig. 7 it could be seen, that only for a short time during the accident, the value for the recombination rate is somewhat higher.

Fig. 8 illustrates the pressure behaviour of both the calculations for a number of characteristic compartments without (upper curve) and with the recombiners (lower curve). At first, the pressure for the case with recombiners is slightly higher than without the recombiners. Here the additional energy input from the catalytic reaction becomes noticeable.

However, the longterm pressure behaviour for the case with recombiners slides down and remains lower than the pressure curve of the reference calculation. During the recombination the non-condensable gases (O_2 and H_2) are transformed to condensable steam. The increased condensation of steam at the surfaces of structures (concrete) as well as the increased heat transfer at the structures due to a higher temperature difference leads finally to a somewhat lower pressure level in the containment.

Fig. 9 depicts exemplarily the temperature behaviours for the middle and highest part of one steam generator compartment (loop 1/2) and for the lower and upper stairways (loop 1). The comparison of both cases show a similar behaviour even though the calculation with recombiners lies higher. In addition to the energy release into the rooms due to the accident propagation, further energies are transmitted to the rooms by the process of the catalytic recombination. But also the compartments without recombiners will be influenced accordingly due to the convection flows between the different compartments, promoted by the function of the recombiners.

For the following discussions, a number of characteristic rooms have been selected where the local effectivity of the catalytic recombination is illustrated. Fig. 10 shows the concentration behaviours within a ternary diagram for the reactor room with and without recombiners. This room is located directly above the reactor and was equipped with a large size recombiner. The dashed line shows the behaviour without and the dragged line with the recombiner.

The distance between the marking points on the curves is equivalent to a time interval of one hour. As it is evident the concentration behaviour without the recombiner proceeds towards the boundary for detonable mixture compositions, while with the recombiner the hydrogen concentration never exceeds the limits of combustion.

A similar behaviour is indicated at the highest dome compartment for which the concentration behaviour is given in fig. 11. A large recombiner is arranged here which reduces the hydrogen concentration in such a way that no longer the limits of deflagration can be reached. In addition a stronger increase of the steam concentration in that area is observed.

Examples given are typical for the behaviour of concentrations where during the progress of the accident and coming from a steam rich mixture composition, the H_2 -concentration reaches and even exceeds the deflagration limits and approaching the boundary of detonation. The positioned recombiners improve the situation distinctly.

The situations inside additional characteristic rooms like steam generator compartments are given in figs. 12 and 13. The lower steam generator compartment (loop 1/2), where a large

sized recombiner is placed, showed similar concentration behaviours like the highest part of the steam generator compartment (loop 1/2). Two small sized recombiners were placed here. Original dangerous hydrogen situation will thus be distinctly improved with the recombiners inside the steam generator towers.

Further characteristic compartments, where without recombiners an extreme H_2 -situation has to be expected are those of low steam concentrations, reaching rapidly the boundary of detonable gasmixtures. Figs. 14 and 15 illustrate the typical rooms of stairways where by the installation of a small recombiner spectacular improvements are achieved.

Another room where the longterm hydrogen situation can be anticipated as dangerous is the so-called tube channel (fig. 16) designated as a dead-end-zone, located outside the missile protection wall. If hydrogen and steam would reach there, then the steam could rapidly condense at the cold walls of the containment leading to high local hydrogen concentrations. In this tube channel no recombiners have been placed in the RALOC nodalisation until now. In spite of that through the installed recombiners - primarily in the equipment rooms - an essential improvement of the hydrogen situation can be noticed. A hydrogen deflagration cannot, however, be excluded in these areas due to the concentration behaviour.

This is also valid for the air ventilation room (level 8) as illustrated in fig. 17. A small recombiner is installed here. This is not sufficient to keep the H_2 -concentration below the deflagration limits. The last two examples are given to make it clear where improvements concerning the installation of recombiners can be undertaken.

As the ternary diagrams illustrate the time dependent behaviour of H_2 -concentration only on the basis of given marks, so in figs. 18 and 19 the time dependent behaviour of H_2 -concentration for the highest dome compartment and the reactor room are given again with and without recombiners.

From these figures, the effectivity of the recombiners to reduce the H_2 -concentrations will be evident.

From fig. 20 the consumption of oxygen by the catalytic reaction could be seen. This figure demonstrates the oxygen depletion in the late phase of the accident leading to an inert situation of the atmosphere, so that the formation of combustible gas mixtures are prevented. Some specific informations on the behaviour of a local recombiner (position: reactor room) are given in fig. 21. The picture of the hydrogen concentrations illustrates the effectiveness of the recombiner, while the figure for the temperatures gives an impression about the temperature of the catalytic plates and the heating up of the surrounding structures of the housing of the boxtype recombiner. These informations are of importance to discuss the questions, whether a catalytic device could be an ignition source for a combustible gasmixture in the area of the position of the recombiner.

7 Preliminary Findings and Further Investigations

The ND* sequence, being analyzed, is characterized by the longtime evolution of the accident, leading to a late release of hydrogen into a containment atmosphere with a high content of steam. Some main results are the following:

- only in some local areas of the containment combustible gasmixtures are formed for a limited time span
- the margin of 10 Vol.-% of hydrogen is exceeded only in very limited areas in the time period being analyzed
- in the longtime more hydrogen is released into the containment than being recombined
- the pressure level in the containment is less compared to the sequence without any catalytic recombination due to the mol-reduction and steam condensation
- in general the temperature level is higher due to the exothermic reaction of recombination
- at the end of the first day the effectiveness of the recombiners is limited due to oxygen depletion
- the consumption of the oxygen by the catalytic reaction leads to an inertisation of the containment atmosphere (< 5 Vol.-% oxygen) so that in the longterm of the accident no combustion of hydrogen could occur. This would be of main importance for the longterm situation in the containment system.

But there is still the need for further investigations to achieve the basic knowledge necessary for the development of generic guidelines for the implementation of a catalytic recombiner system in a large dry containment.

First of all, some more characteristic severe accident sequences have to be analyzed, e.g. a low pressure scenario (ND-case), resulting from the break of the surgeline or the initial event of multiple steam generator tube failures, propagating into a severe accident. Especially the ND-scenario is characterized by an early core degradation, leading to the release of hydrogen into a containment atmosphere with less steam than in the ND*-case. The selection of different severe accident sequences, covering the boundary conditions for the lay-out of a catalytic recombiner system is still under discussion in Germany. The investigation of the ND-scenario at GRS is still in progress.

In addition, investigations are required to optimize the distribution of catalytic recombiners in the containment for the range of different severe accident scenarios. Also the consequences of a local failure of one or two catalytic recombiners being placed nearby the release location of hydrogen resulting from blow-down forces or strong poisoning of the catalyst have to be analyzed to assure the overall effectiveness of this mitigation concept.

Another open point is related to the presence of a high amount of CO in the late phase of a severe accident, resulting from an extended core-concrete interaction process. Some preliminary experimental results showed, that also the CO will be recombined, when the catalytic material is at an elevated temperature level. But the simultaneous recombination of hydrogen and carbonmonoxid has to be further investigated.

In general, the first calculations, presented above demonstrated that the installation of a catalytic recombiner system is a very effective measure to reduce the level of hydrogen in a containment during the propagation of a severe accident but due to specific local conditions the generation of a combustible gas-mixture could not be prevented by the only use of catalytic recombiners. This leads to the discussion concerning the need for complementary measures to cover the whole range of accidental conditions in a large dry containment. In Germany the additional implementation of a limited number of battery powered igniters at selected positions in the containment (DUAL-concept) is still investigated.

In late autumn 1996 such an integral system is expected to be proposed by the utilities. It is planned to have discussions on this system in the German Reactor Safety Commission.

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Appendix 1

The Recommendation of the German Reactor Safety Commission, concerning the Implementation of Hydrogen Mitigation Technique

The following recommendation was decided by the German Reactor Safety Commission and published in the "Bundesanzeiger", date July 14, 1994:

"In order to avoid an early or late loss of integrity of the containment of a PWR-plant due to hydrogen deflagration during beyond design basis accidents, the Reactor Safety Commission (RSK) recommends the application of catalytic recombiners. They recombine hydrogen before it reaches the deflagration limit as well as from steam inerted gas mixtures. Thus, a safety related meaningful amount of released hydrogen can be recombined within some hours and thereby contributions towards assuring the integrity of the containment as well as for the reduction of risk can be made. Catalytic recombiners are to be designed to achieve the efficiency. The catalytic recombiner is an unequivocal safety related measure to control hydrogen during beyond design events.

For the catalytic recombiners, concepts of prototype are existing which are technically well developed and proven by tests. Recombiners are passive constructional elements. Neither do they require any service by the operators nor do they require any energy supply. The installation of these recombiners within the existing PWR-plants does not pose any safety related problems.

RSK is proposing to optimize the constructional details concerning the specific forthcoming application. Test samples from catalytic devices should be tested annually to demonstrate the catalytic activity. Regarding the number and the location of catalytic recombiners, the release rate of hydrogen as well as the characteristic gas transport times within the containments are to be considered.

These recombiners are to be placed primarily in the neighborhood of global convection flows of the containment, near the containment steel shell as well as in those compartments where hydrogen will be released. Complying the physical principles within the voluminous containment, large scale convection loops will be formed. With numerical calculations and engineering judgment and on the basis of existing knowledge regarding the distribution of hydrogen, the required number and location of the recombiners can be determined with sufficient exactness.

For the installation of the catalytic recombiners, the RSK is expecting quick preparation and submission of concrete technical plans from the utilities and vendors. Hereby the enveloping course of events can be based on the accident scenarios described in the introduction (late recoverable severe accidents with up to 100 % of Zirconium oxidation, low pressure core melt scenario with core concrete interaction).

As for further step the RSK will examine the requirements to supplement the catalytic recombiners through fixed early burning of hydrogen by the igniters having short path of flame propagation or through post dilution of the containment atmosphere. For the application of igniters, the RSK thinks it is necessary to prove the transferability of the recently obtained results and also the results of the planned tests on hydrogen combustion on the real conditions of a PWR plant."

The BMU shares the opinion of the RSK and has formally asked the state authorities to inform the utilities correspondingly.

Table 1 Main events of the ND*-accident sequence

Time	Main Events of the ND*-Sequence - Plant Specific Case -
0,0 sec	initiating event; loss of secondary feed water supply and loss of secondary heat sink during normal plant operation
0,5 - 11,0 sec	primary pressure limited by pressurizer spray system
14,0 sec	reactor scram; begin of controlled secondary cooldown
~10 min	end of controlled secondary cooldown
~20 min	shut-off of MCP (hand action)
~28 min	MCP stopped, begin of natural circulation in primary circuit
~55 min	SGs are empty
~1 hr 10 min	1. opening of pressurizer relief valve (steam release)
~1 hr 20 min	opening of pressurizer relief tank and begin of steam release into containment; shut-off of containment and annulus air ventilation system; shut-on of annulus accident air ventilation system
~1 hr 45 min	saturation conditions reached in the whole primary circuit
1 hr 58 min	initiation of primary bleed action (RPV min-3) by opening of all pressurizer relief and safety valves (total relief area 100 cm ²)
~2 hr 05 min	water level reaches bottom level of active core
~2 hr 05 min	cold leg accumulators shut-off by plant specific regulations
~2 hr 10 min	begin of 1. accumulator injection phase into hot legs followed by partial core cooling (~40 % of total capacity)
~2 hr 50 min	begin of 2. accumulator injection phase into hot legs followed by partial core cooling (~25 % of total capacity)
~3 hr 30 min	begin of core melting in upper core cells of all rings; melting front goes from inner to outer ring and from upper to lower part of the core
~4 hr 10 min	last accumulator injection phase into hot legs, partial cooling of destroyed core (accus are empty after ~10 min)
~5 hr 48 min	failure of core support plate, core material slumping and quenching in the low. plenum
~6 hr 28 min	RPV failure and begin of melt release into reactor cavity
~7 hr	increasing of MCCI after heat up of released melt
~17 hr	sump water ingress into reactor cavity because of the radial erosion front reaches the outer cavity wall
~48 hr	initiation of accident filtered venting of containment at 0.6 MPa
~75 hr	stop of accident filtered venting of containment at 0.35 MPa
84 hr	end of calculation

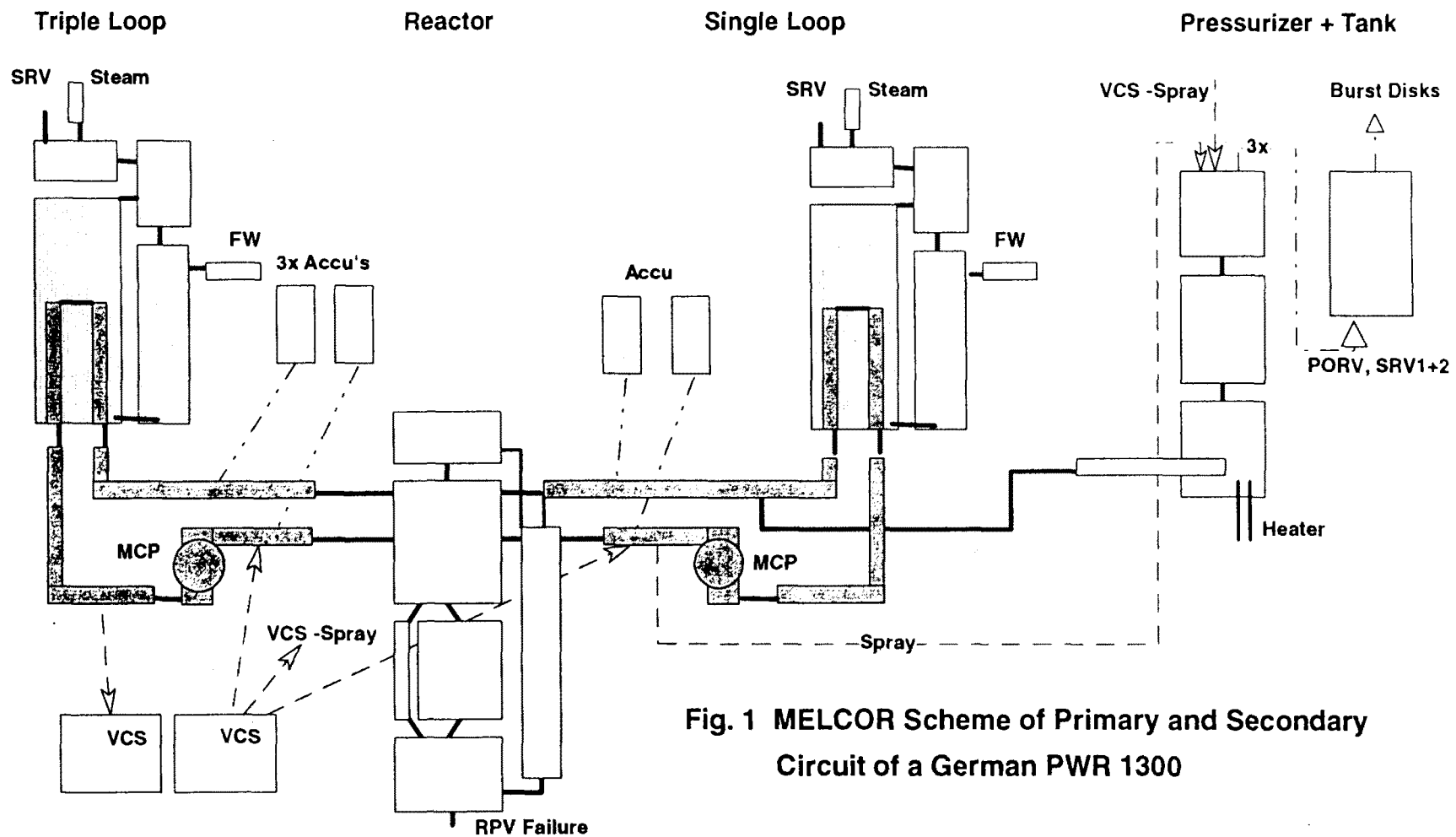
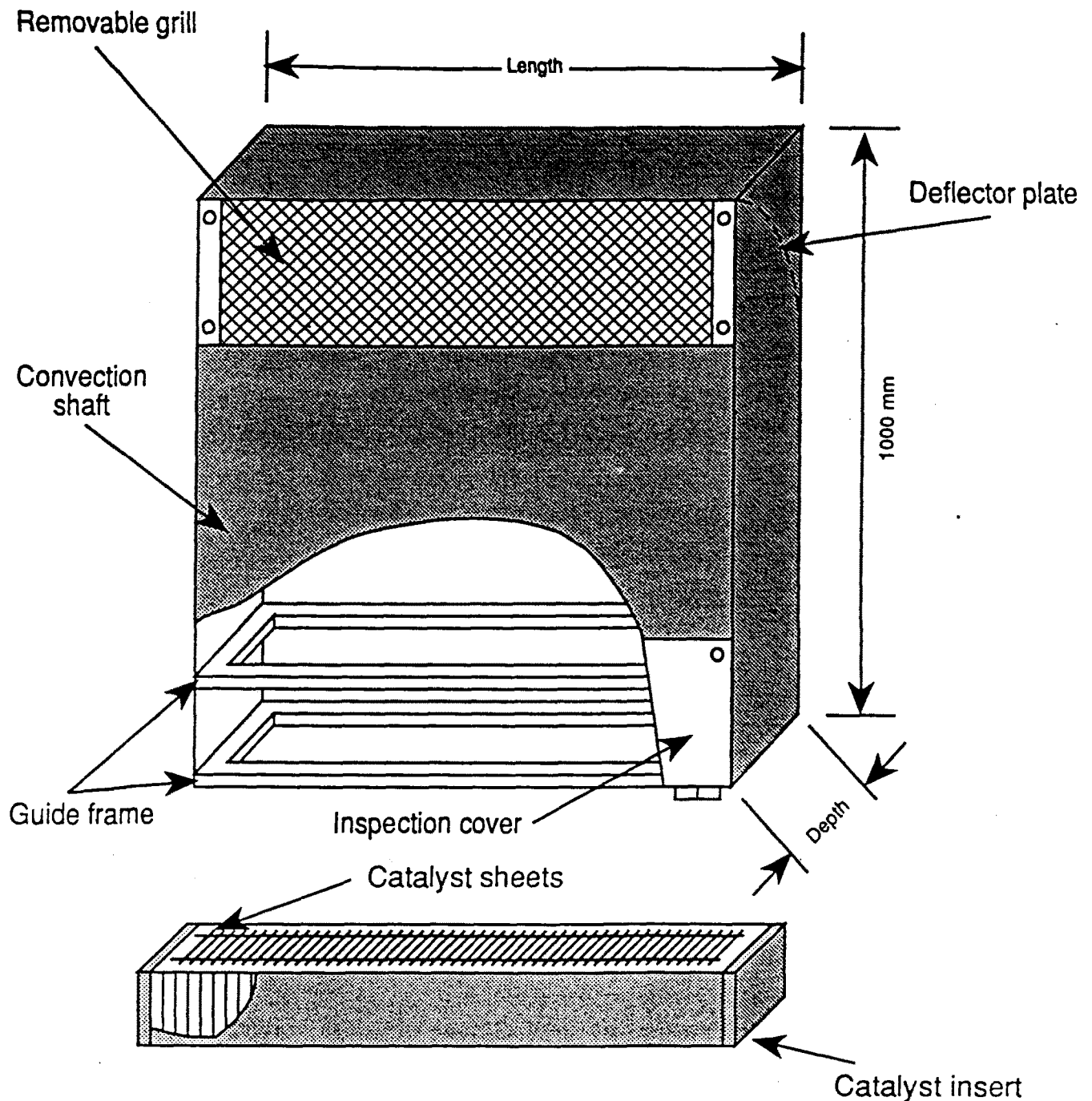


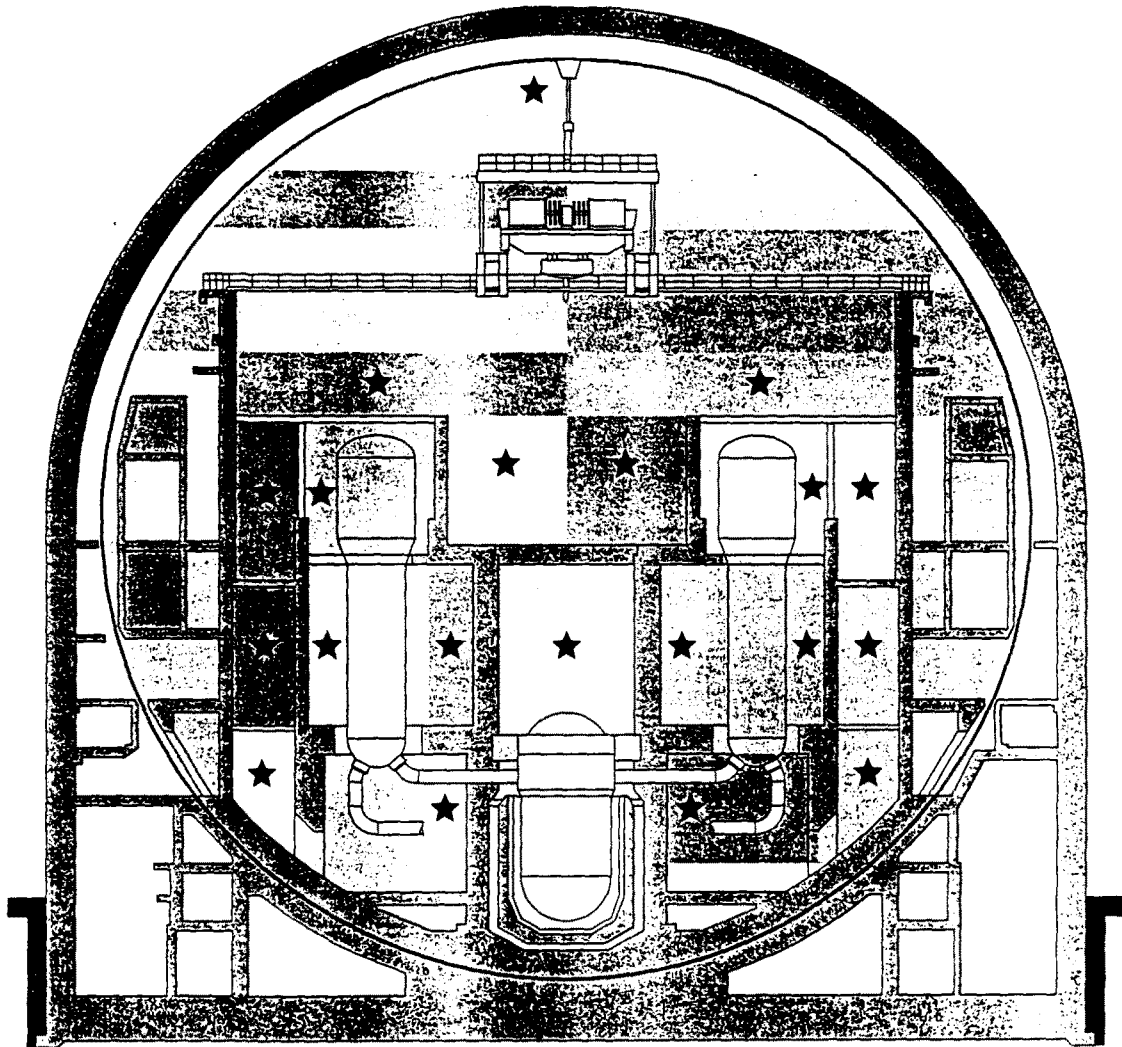
Fig. 1 MELCOR Scheme of Primary and Secondary Circuit of a German PWR 1300



Available Sizes:

Type	Length (mm)	Depth (mm)
FR 90/1-150	150	150
FR 90/1-320	400	150
FR 90/1-960	1000	150
FR 90/1-1500	1500	300

Figure 2 Siemens Catalytic Box-type Recombiner
(ref.: R. Heck, Siemens, SMIRT 12 Conference 1993)



★ marks the position of catalytic recombiners

Figure 3 **RALOC-Nodalisation Scheme of a 1300 MW PWR
Containment Type Konvoi**

1300 MW-PWR, KONVOI -Type

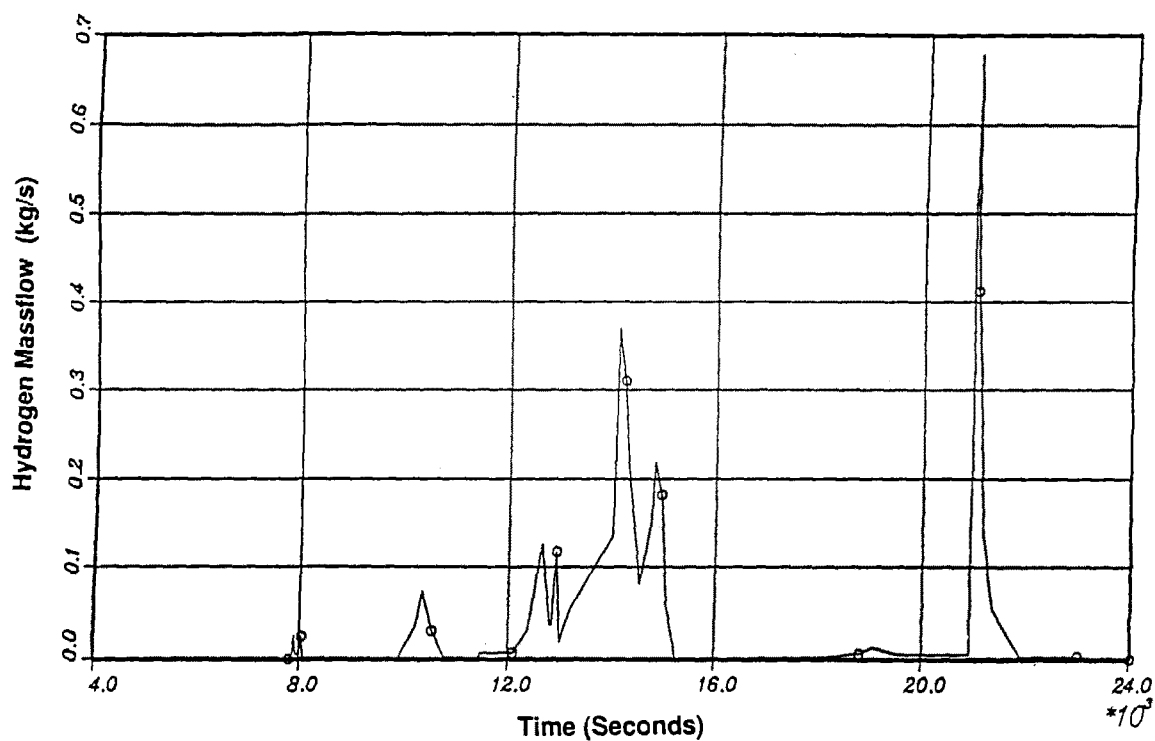


Fig. 4 Hydrogen Release into the Containment (in-vessel-phase) during the ND*-Accident Sequence

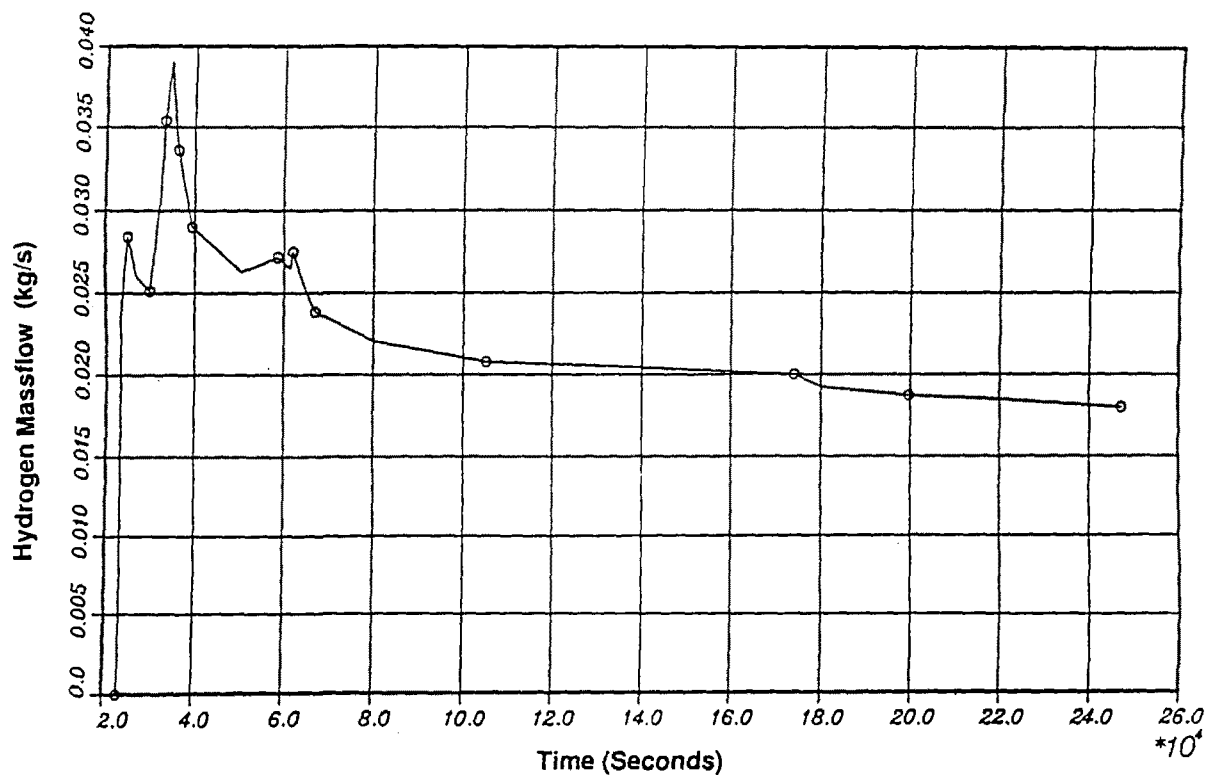


Fig. 5 Hydrogen Release into the Containment (ex-vessel-phase) during the ND*-Accident Sequence

1300 MW-PWR, KONVOI -Type

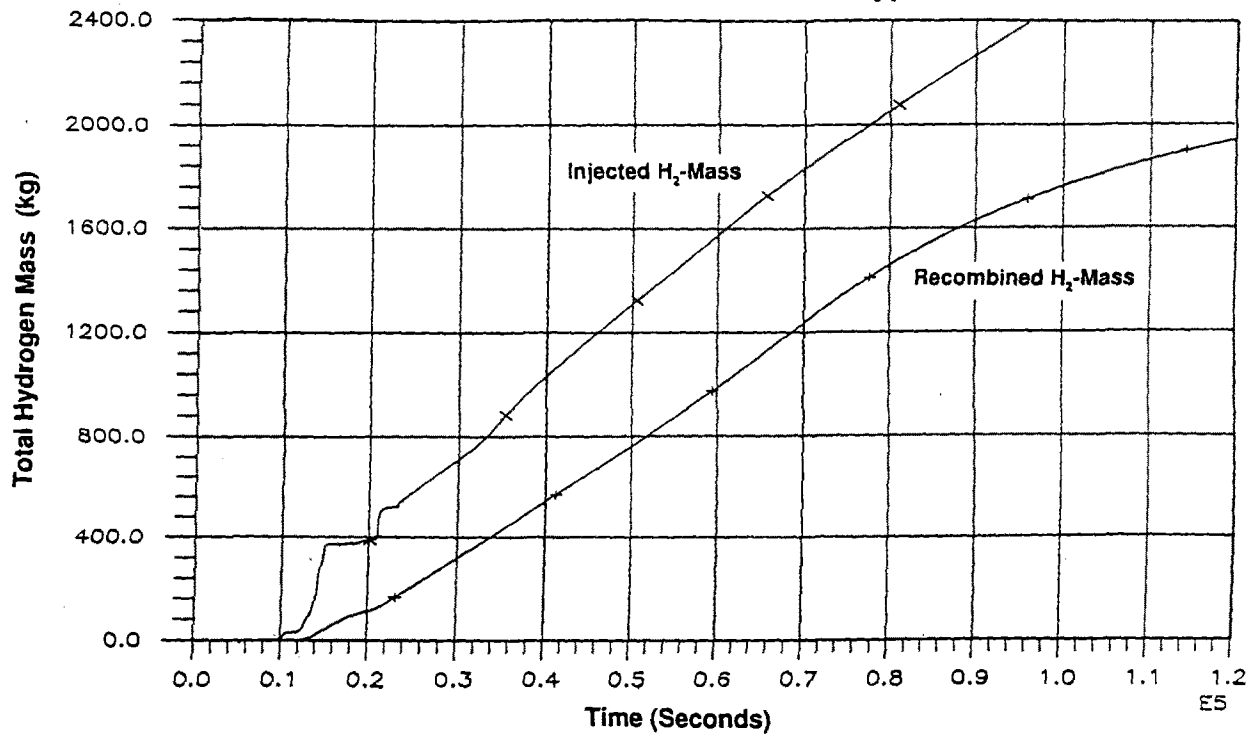


**Fig. 6 Total Mass of Hydrogen being released into the Containment
and recombined during the ND*-Accident Sequence**

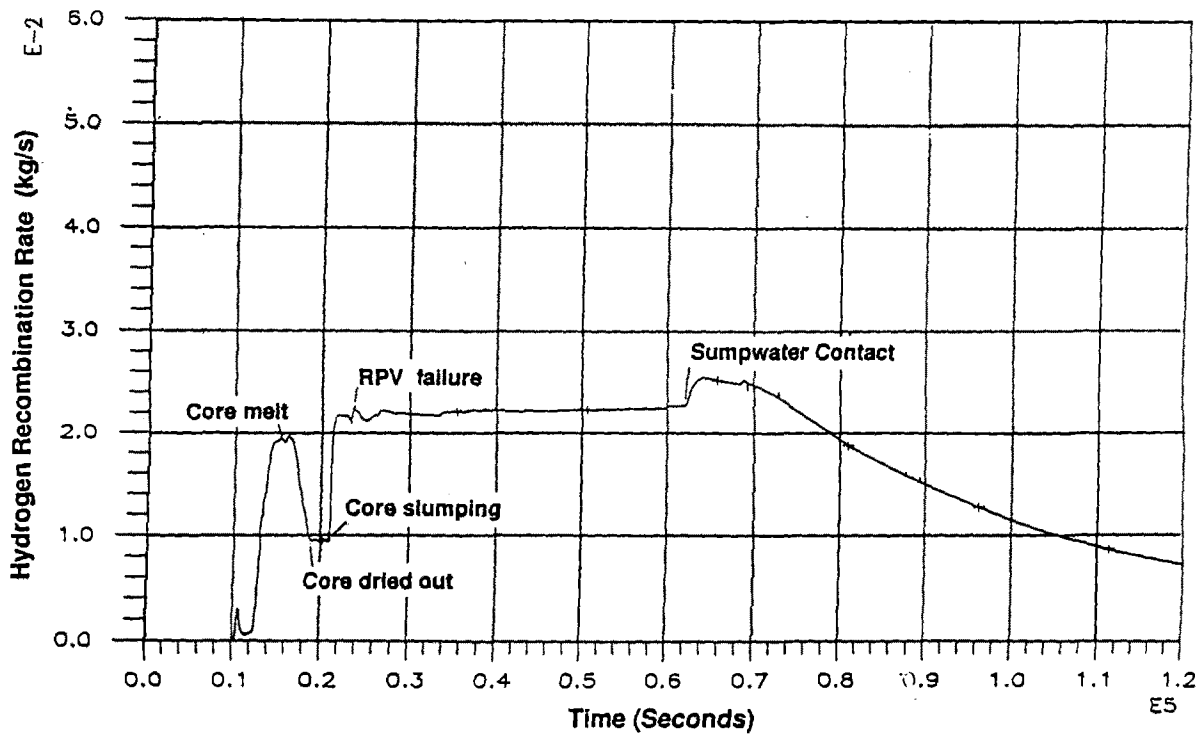


Fig. 7 Total Hydrogen Recombination Rate during the ND*- Accident Sequence

1300 MW-PWR, KONVOI-Type

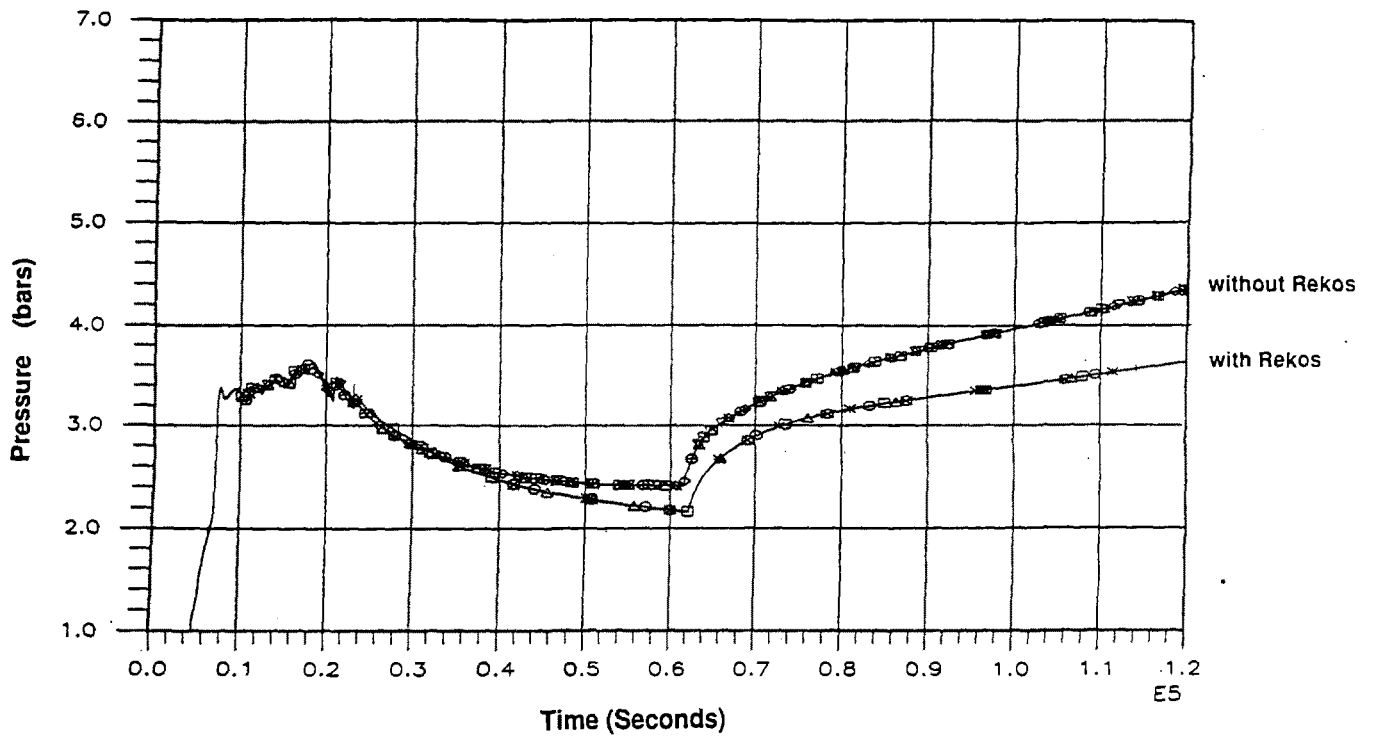


Fig. 8 Containment Pressure during the ND*- Accident Sequence
with and without the Effect of catalytic Recombination

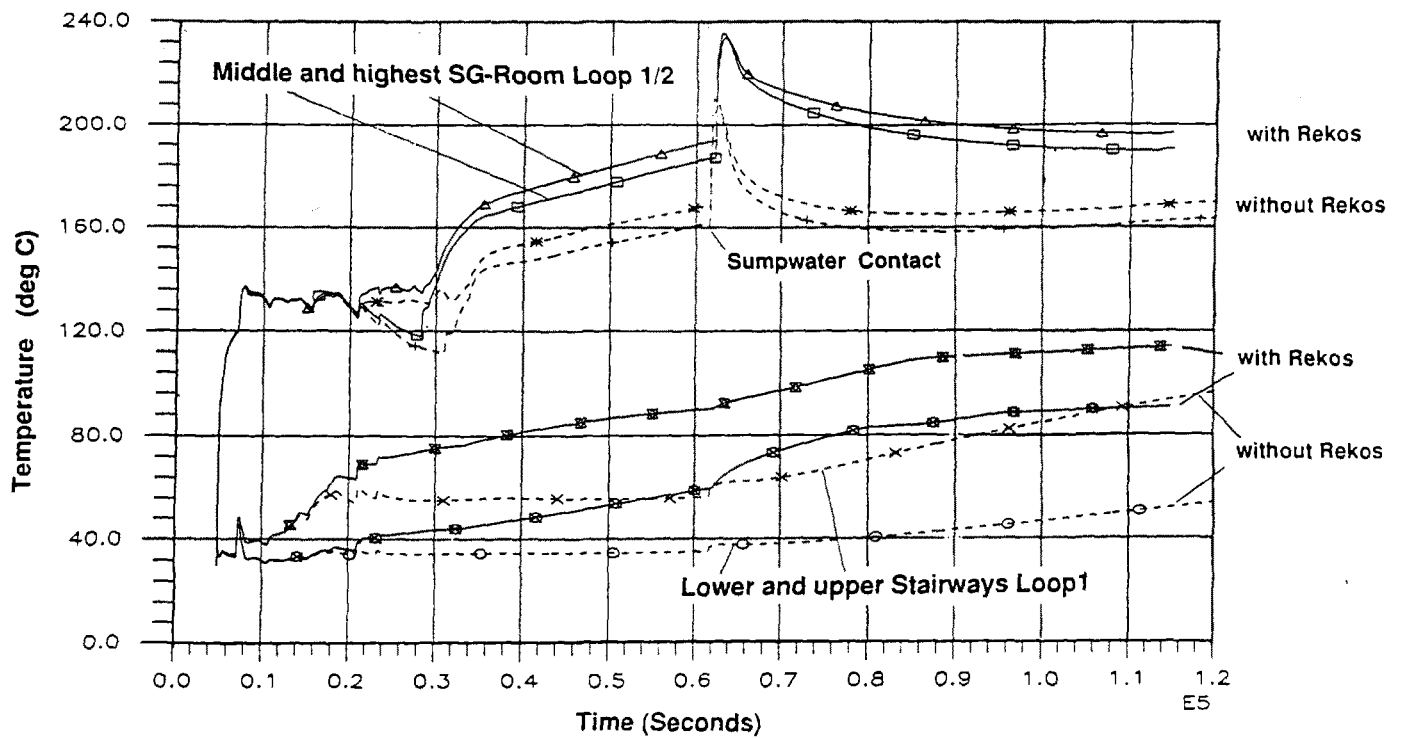
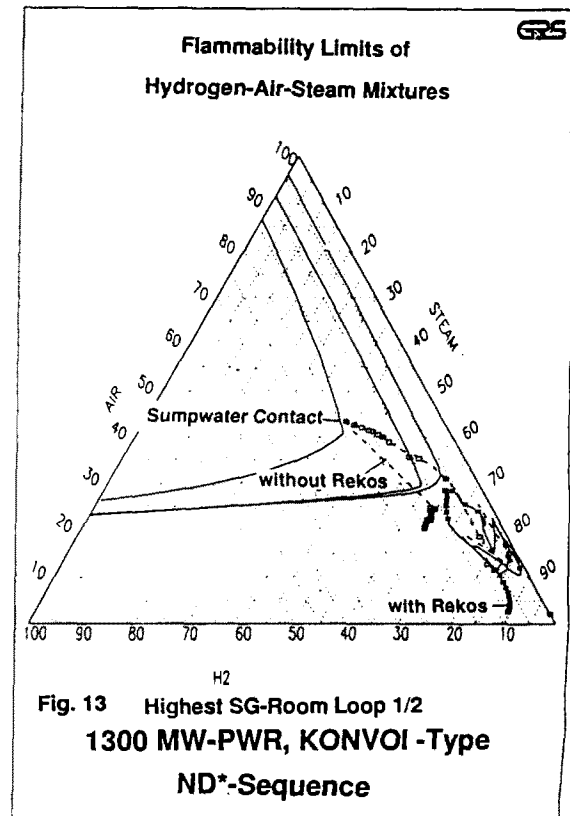
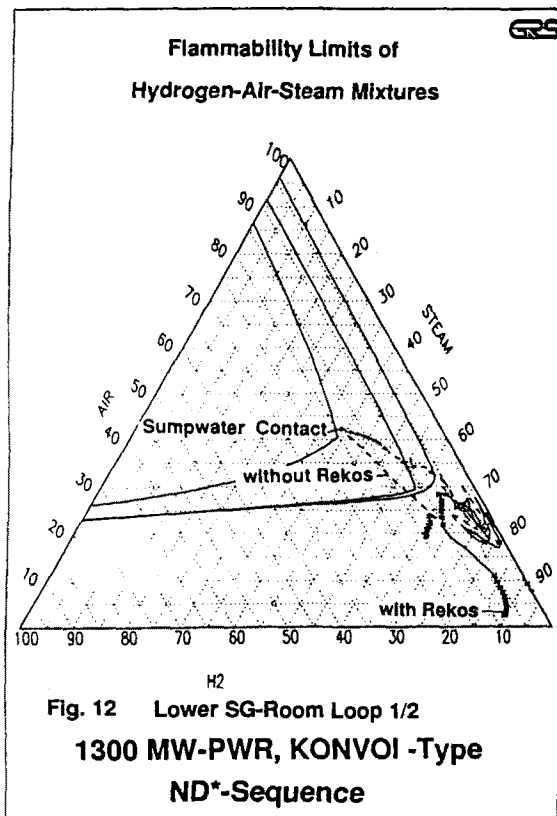
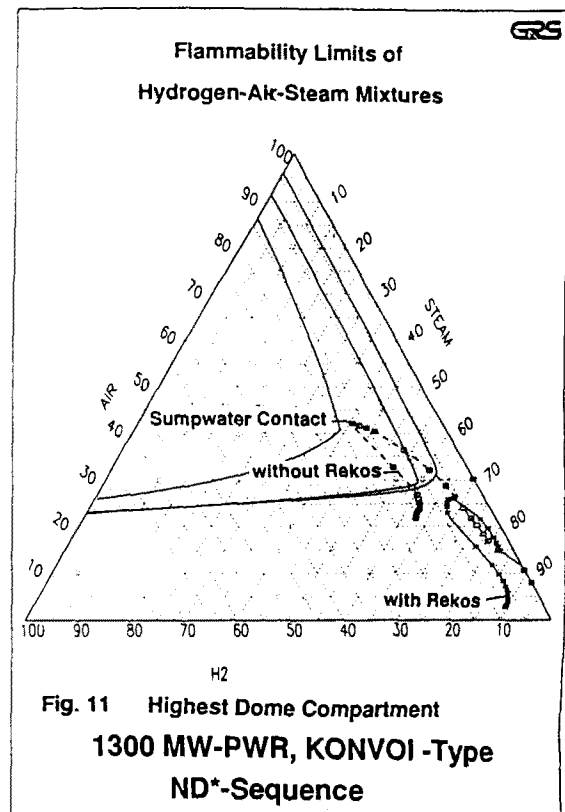
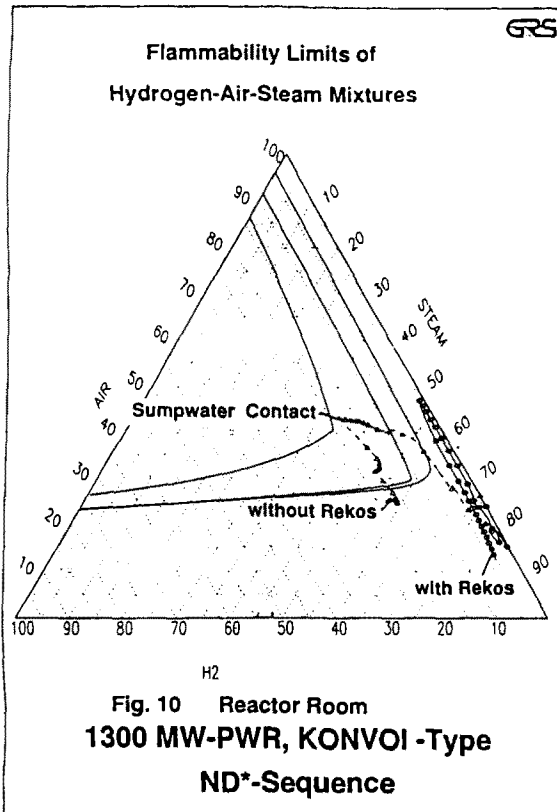
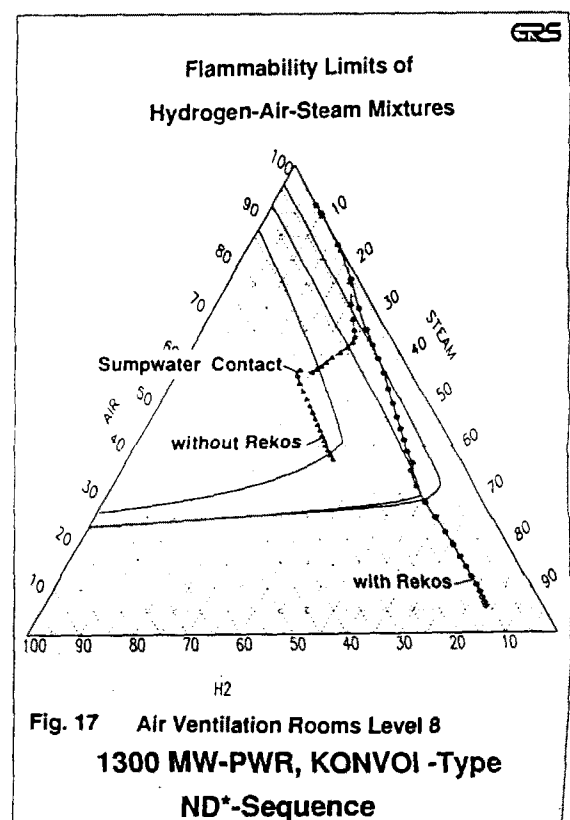
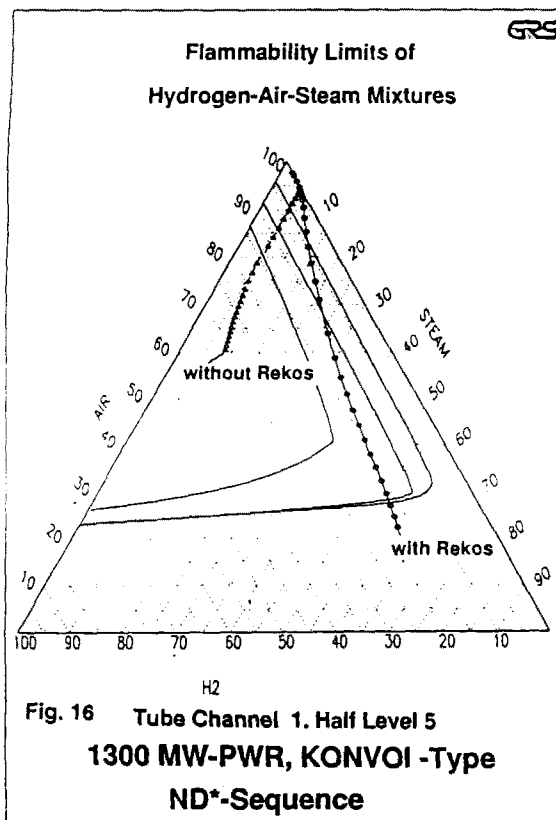
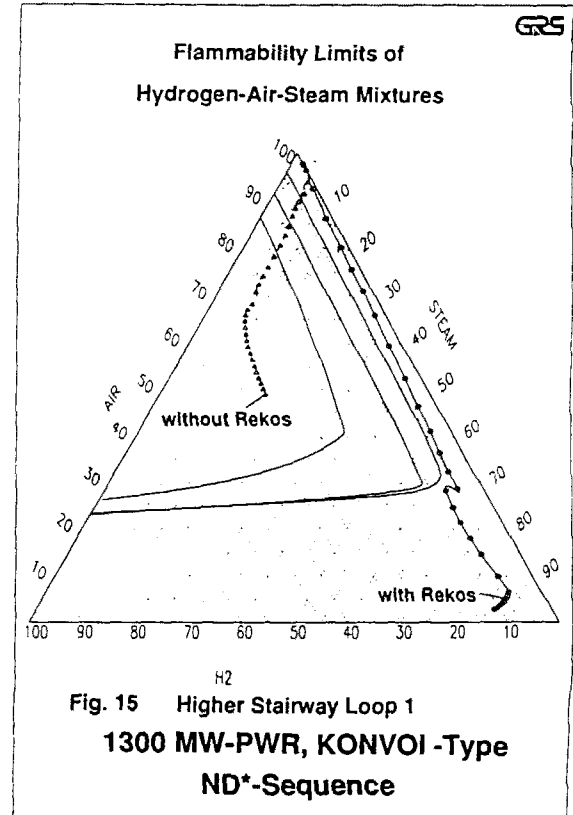
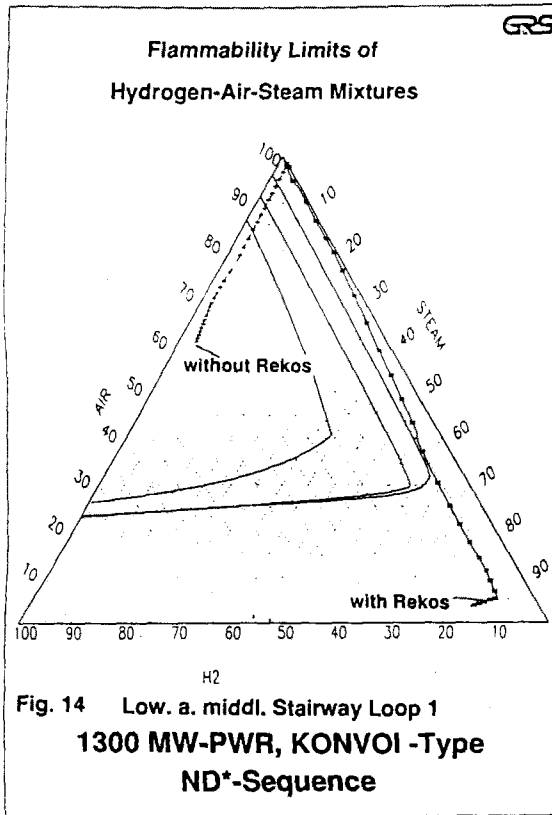


Fig. 9 Containment Temperatures during the ND*- Accident Sequence
with and without the Effect of catalytic Recombination





1300 MW-PWR, KONVOI -Type

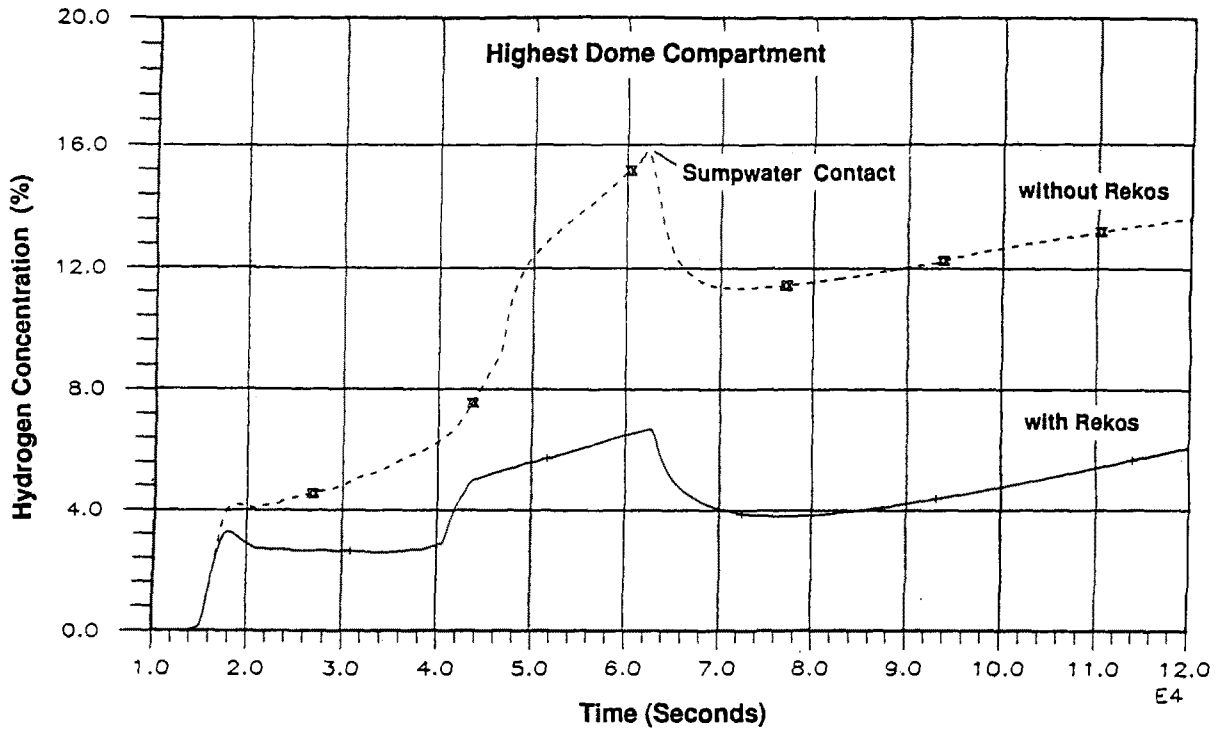


Fig. 18 Hydrogen Concentrations during the ND*- Accident Sequence
with and without the Effect of catalytic Recombination

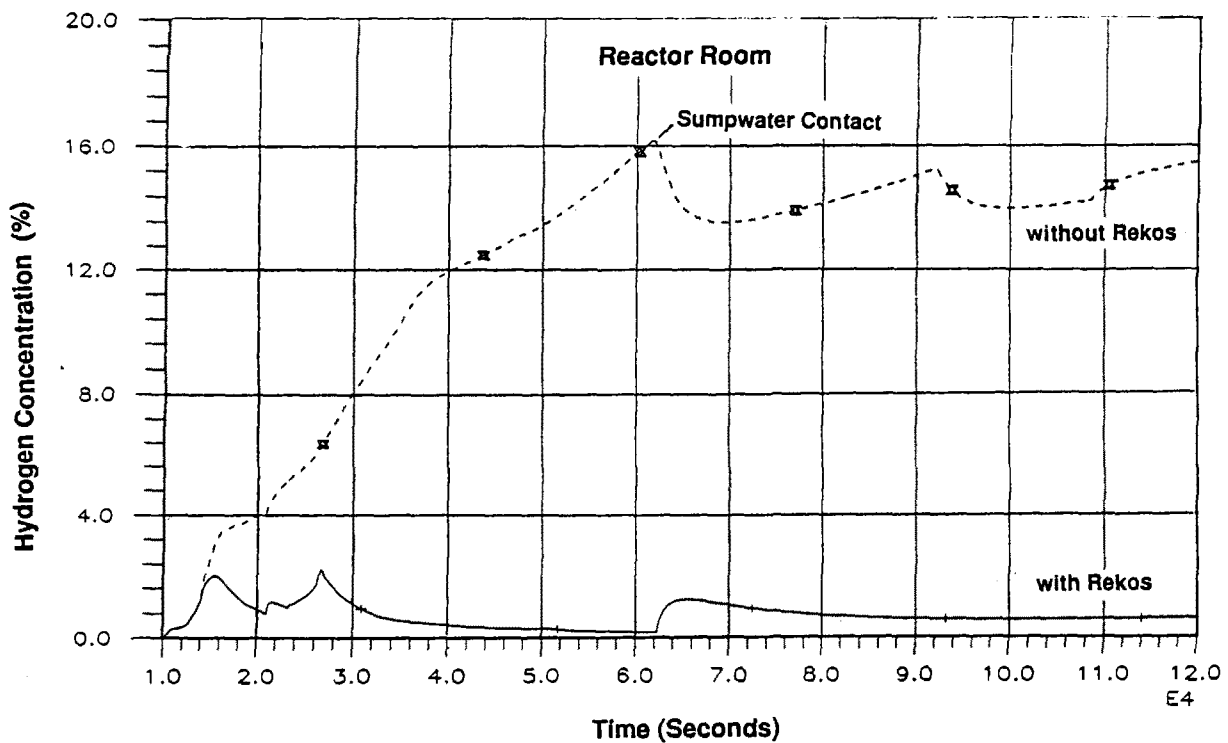


Fig. 19 Hydrogen Concentrations during the ND*- Accident Sequence
with and without the Effect of catalytic Recombination

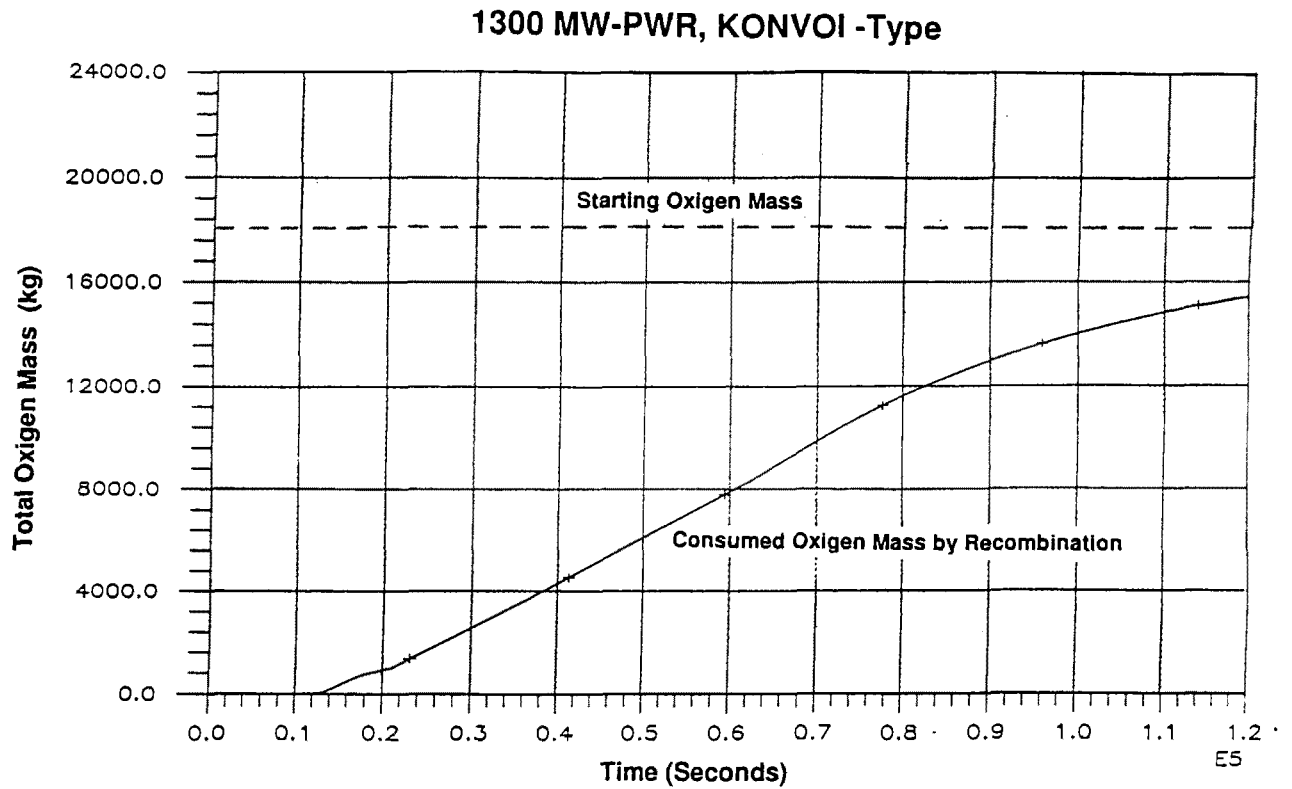


**Fig. 20 Consumption of Oxygen by the catalytic Recombination
during the ND*-Accident Sequence**

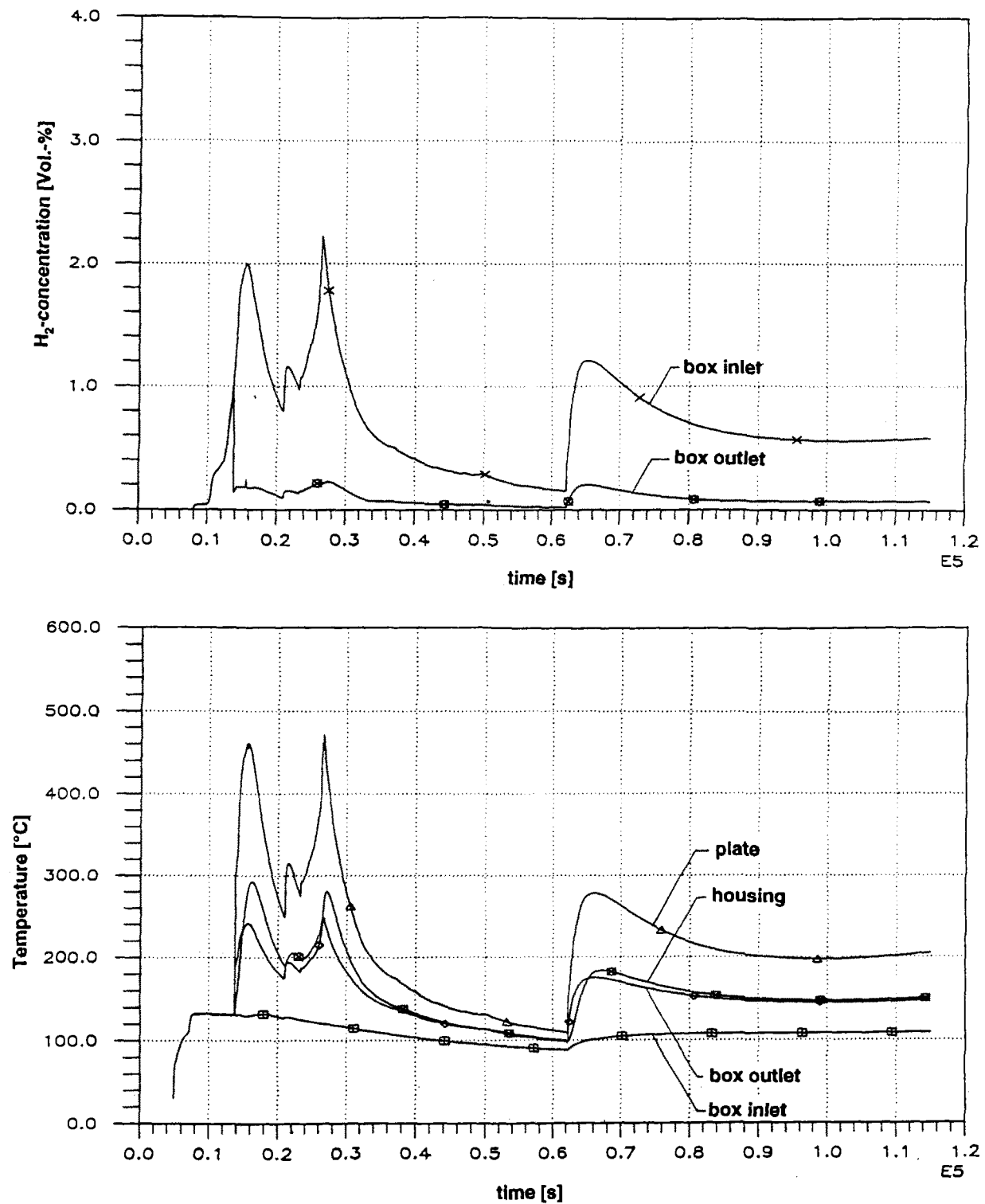


Figure 21 Hydrogen Concentrations and the Temperatures of Gas, Catalytic Plate and Housing for a Catalytic Recombiner (position reactor room)

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3.7 FIRST EXPERIENCE WITH THE INSTALLATION OF PASSIVE AUTOCATALYTIC RECOMBINERS

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Abstract

From the principle decision of installing Passive Autocatalytic Recombiners (PARs) in all Belgian NPPs to the installation on site, several problems needed to be identified and solved; it is the goal of this paper to address these issues and to present the solutions which were adopted.

The first section deals with the sizing and the distribution of the catalytic surface inside the containment to be equipped. Rather than to rely on numerous multicompartment calculations, the procedure is based on a single volume approach combined with engineering judgement. The criteria applied in the successive design steps are explained and illustrated for the Doel1 unit.

The second section is devoted to additional requirements concerning the resistance to poisoning agents and the maintenance constraints, and to some recommendations for the design of the supports.

Because the effectiveness of the PARs entirely relies on the catalyst material, a periodic control of the active parts is of primary importance. Therefore, the Transportable In-Service Inspection Equipment (TIIE) and the test procedures are presented in the third section.

Finally, recommendations are made for further work, derived from this first experience.

1. Introduction

The PSAs carried out for the Belgian NPPs of Doel3 and Tihange 2 had identified the possible modes of containment failure. It was concluded that the combustion of hydrogen and carbon monoxide mixtures can become in some accident sequences a challenging phenomenon for the containment leaktightness.

Therefore, different preventive and mitigative means have been examined to cope with the hydrogen risk: plug ignitors, catalytic ignitors, catalytic recombiners, containment pre- or post-inertisation. The pros and the cons of those different solutions have already been presented in different papers [1, 2] and will not be repeated here.

As a result of that comparative study, it was decided to examine in more details the performances of the Passive Autocatalytic Recombiners (PARs) [3]. This investigation concluded that:

- these recombiners are totally passive and self-starting ;
- the recombination starts at low concentrations of hydrogen even in steam inerted atmosphere and the recombination rate increases with increasing concentrations;
- the recombiners operate as long as the oxygen concentration is greater than or equal to the value derived from the stoichiometric equation;
- the recombiners promote natural convection and hence contribute to the homogenisation of the containment atmosphere; this prevents the build-up of high concentration pockets and reduces the risk of local detonations;
- the recombiners are resistant to poisoning agents such as elemental iodine, organic iodide, boron, welding fumes, smoke from oil and cable fires.;
- as the recombiners rely on a simple working principle, they only require limited installation and maintenance work.

Because no adverse effect has been identified, it was concluded that this equipment is safety oriented and cannot worsen the conditions resulting from a highly hypothetical severe accident.

Therefore, the Belgian Utility Electrabel came to the principle decision to install Passive Autocatalytic Recombiners in all units and asked Tractebel to carry out the project. The call for bids was issued end 1994 and Siemens was finally selected on basis of technical and economic quantitative criteria. A picture of the selected equipment is shown on Figure 1.

The objective of this paper is to present the different steps which were performed from the principle decision to the commissioning of the recombiners in the Doel1 NPP in October 1995.

2. Sizing and distribution of the catalytic surface

Doel1 is a Westinghouse two-loop reactor of 420 MWe which was commissioned in 1974. The primary containment is made of a metal sphere of 42870 m³; the inner compartments are connected to each other through large openings which allow the development of natural convection loops and a fast homogenisation of the atmosphere in case of accident. That homogenisation capability is still enhanced when the fan coolers are operational.

Hence, rather than to rely on numerous multicompartiment thermalhydraulic calculations which are subject to many uncertainties for the time being, it was decided to apply the results of the PSA of Doel3 in a single volume approach, combined with simple design rules based on engineering judgement. That decision was further supported by the geometric constraints and the maintenance requirements which have to be taken into account in the backfitting of an existing plant. Because, the possibilities to install catalytic recombiners in most of the compartments are very limited, any optimisation process should probably lead to a similar implementation.

The successive steps of the design procedure can be summarised as follows:

- a) sizing the total catalytic surface in a reference unit to satisfy a prescribed criterion;
- b) extrapolating the result to the unit under consideration;
- c) distributing the total surface within the different compartments of the reactor building;
- d) checking that oversizing is adequate in order to cope with the uncertainties inherent to this kind of calculation.

In step (a), the average hydrogen concentration in the containment is calculated for the most penalising accident sequence in the reference plant. From the results of the PSA performed for Doel3, it appeared that the relevant scenario is the sequence SLY which refers to a small break LOCA with failure of the ECCS when switching over to recirculation and with the containment heat removal system operating. This scenario results in a high in-vessel hydrogen production because the core stays at high temperature over a long period of time with a maximum inventory of water available for steam production. As the containment heat removal system is running, steam condensation leads to high hydrogen concentrations.

Using the thermalhydraulic data and the hydrogen production rate obtained with single volume calculations (MARCH3), the total catalytic surface has been sized with the program CARE developed in Tractebel. It was required that the average hydrogen concentration should not exceed 5 % at any time of the sequence SLY, in order to preclude any generalised deflagration of gas mixtures. Based on validated performance data of the recombiners, the program CARE allows to compute the concentrations of the different gases in the containment with and without a given catalytic surface; it also provides the adiabatic isochoric complete combustion pressure in both cases. Because the program CARE has already been presented in other references [4,5], no details are given here. In the particular case of Doel3, this first design step leads to a total surface S_{5%} equal to 250 m² (Fig.2).

When no detailed severe accident calculations are available for the plant to be equipped, a simplified approach is needed in step (b) to apply the results of a reference plant to the unit under consideration. This was the case for Doel1 and it has been assumed:

- that the thermalhydraulic conditions are identical in both units at any time of the accident sequence;
- that the hydrogen release rate is proportional to the zirconium inventory in the core.

In other words, both plants behave similarly when experiencing the same accident. Although this assumption cannot be strictly justified, it introduces uncertainties which are probably not higher than those associated with the plant specific calculations of the accident sequences. Based on engineering judgement, the reference surface is extrapolated using the respective zirconium masses (Zr) and containment volumes (V_{tot}) through the formula:

$$\frac{(S5\%)}{(S5\%)_{ref}} = \frac{(Zr) \times (V_{tot})_{ref}}{(Zr)_{ref} \times (V_{tot})} \quad (1)$$

This extrapolation results in a minimal surface of 176 m² in order not to exceed the 5% concentration criterion in the Doel1 plant. Confirmatory calculations performed later with an adapted version of the program CARE have shown that the relationship (1) produces conservative results.

Once the total catalytic surface is known for the plant under consideration, it must be distributed within the different compartments according to the following installation rules.

First, the recombination process obeys an exponential law characterised by a time constant λ depending on the features of the recombiner and on the thermalhydraulic conditions. This time constant can be written under the form:

$$\lambda = C \frac{S}{V} \quad (2)$$

where C depends on the recombiner and on the conditions, while S and V respectively denote the catalytic surface and the associated volume. Alternatively, the process can also be characterised by a time constant:

$$\tau = \frac{\ln 2}{\lambda} \quad (3)$$

which is the time needed to decrease the hydrogen concentration by a factor 2 in the absence of hydrogen production.

It is imposed that the time constant shall not be locally higher than the value derived for the whole containment. Because the conditions are assumed homogeneous in the reactor building, the condition can be expressed using equation (2) through the relationship:

$$\frac{S_i}{V_i} \geq \frac{S5\%}{V_{tot}} \quad (4)$$

which allows to derive the catalytic surface S_i required in a compartment with a volume V_i from the global values $S_{5\%}$ and V_{tot} .

However, only three models of PARs are available, the FR-320, FR-960 and FR-1500, with active surfaces respectively equal to 1.07m^2 , 3.22m^2 and 11.76m^2 (see Table1). Therefore, the catalytic surface which is installed in a given compartment is often higher than the surface required by criterion (4). And because it is less expensive to install one FR-1500 than two FR-960, an economical optimisation results in a further oversizing of the surface.

When applying the distribution rule (4), it is recommended to also account for the location of the potential sources of hydrogen (pressurizer, relief tank, steam generators...) to concentrate the extra recombination capacity in the rooms where the hydrogen concentration could be temporary higher than the average value. On the other hand, the location of the recombiners in the containment dome must be optimised according to the possible flow paths resulting from the natural convection and from the forced convection induced by the fan coolers.

Besides the functional criteria, the recombiners must also satisfy accessibility criteria and may not hamper the maintenance activities inside the reactor building. A good accessibility to the drawer is required to allow an easy extraction of the catalytic plates for the in-service inspection (see section 3). The recombiners may not interfere with the normal operation of the plant. In particular the safety criteria during operation and shut-down must be fulfilled and the safety of the workers must be assured.

Although each of the above mentioned criteria can easily be applied separately, the final layout is often a compromise which needs to account for many geometric constraints.

But generally, step (c) of the above mentioned procedure results in an installed surface which is approximately 15 to 20 % higher than the value derived to satisfy the 5% criterion.

Finally, the last step (d) of the design procedure consists in a final check on the installed surface; according to a recommendation of the Belgian Safety Authorities, the effective surface has to be 20% larger than the value derived from the 5% criterion, which is expressed by the condition:

$$\sum S_i \geq 1.2 * S_{5\%} \quad (5)$$

This calculation is performed using FR-1500 equivalent surfaces. In the balance expressed by equation (5), the catalytic surfaces of the FR-960 and FR-320 modules are reduced by a factor 1.5 to account for the less favourable natural convection conditions inside narrower housings where the side wall boundary layers have a relatively higher impact on the internal flow.

It is worthwhile to note that the 20% design margin is introduced to cope with the inherent uncertainties existing in the knowledge of:

- the course of a severe accident;
- the zirconium inventory in the core which varies with the type of fuel;
- the homogenisation of the atmosphere.

In addition, this margin accounts for the failure of 1 or 2 PARs which could be destroyed by a water jet or by missiles, and for the unavailability resulting from a partial containment flooding.

But because the step (c) of the design procedure already yields a significant design margin, the 20% goal can be easily achieved through minor modifications such as the choice of a larger recombiner in some places.

At last the proposed layout must be discussed and finalised in collaboration with the Utility during a walk-down in the reactor building.

For illustrative purposes, the main data of the procedure as applied to the Doel1 plant are summarised in table 2.

3. Additional design requirements

The recombiners must also comply with Quality Assurance requirements. These requirements apply in particular to the performances of the recombiners and to the resistance against poisoning agents. Validated data must be available about the recombination capacity in various conditions to allow detailed comparative calculations between the quotations. Particular attention must also be paid to the resistance of the catalyst against elemental iodine, organic iodide, cesium, borated water, welding fumes, painting vapours, smoke from oil and cable fires. The results of the relevant qualification programmes need to be examined in detail. In this respect, both efficiency and resistance tests must be certified by an independent organisation.

On the other hand, each catalytic plate must be identified with a batch number and a running number inside the batch. This information together with the associated PAR number has to be provided in a data base to allow an unambiguous identification and an easy management of the parts.

Other aspects are more directly related to the mechanical design.

The catalytic plates have to be removed for the in-service inspection tests. Because some PARs are located in sensitive areas such as the compartments of the steam generators and of the pressurizer, the accessibility to those parts must be as quick and as convenient as possible to comply with the ALARA principle. In the particular case of the recombiners chosen in Belgium the plates are placed vertically in a drawer.

Because the catalyst is made of platinum, a special request was raised by the Utility to protect the plates against theft during the outages. Hence, the inlet section of the recombiners is protected with a grid and screws with a non standard head are used to lock the drawer.

Finally, an important item concerns the design of the supports which must be carried out in accordance with the following guidelines.

First, all supports must resist seismic loads because they are mounted inside the reactor building.

Secondly, the PARs must be laid out in such a way that they do not interfere with maintenance operations and that they do not hamper the access to other components. Hence, they are mounted on various types of structure such as metal frames, concrete floors, walls, ceilings, and different types of support have to be designed. At this stage, particular attention must be paid to optimised standardisation rules and to the problems of accessibility into some compartments. In this respect, it is highly recommended to design standardised and dismountable supports which can be assembled inside the reactor building. Bearing in mind those recommendations will substantially limit the design and calculation efforts as well as the time required for mounting, thus reducing finally the doses to the workers.

The items specific to the in-service inspection are detailed in the next section.

4. In-Service Inspection

Each site is provided with a Transportable In-Service Inspection Equipment (TIIE) in order to perform the in-service inspection in all units (fig. 3 & 4).

The TIIE consists in an oven with a test section in which the catalytic plate is placed. The test section looks like a "waffle iron" and it is equipped with three thermocouples to measure the inlet (T1) and outlet (T3) temperatures, and the surface temperature near the lower edge of the plate (T2). The test occurs at a temperature of 323 K which is representative of the containment conditions in normal operation. Air with 2% hydrogen is continuously injected in the test section and the recombination is detected by the temperature increase of thermocouple T2. The catalytic plate fulfils the performance specification if the temperature increases by 5K within 5 minutes. In case of failure, two additional plates from the same batch are tested to identify if the reason is generic or due only to local conditions.

All recombiners are tested during the first two outages after installation; afterwards the inspection concerns only 5% of the recombiners at each outage. The inspection of an equipment comprises both a visual inspection and a performance test applied to 2% of the catalytic plates in accordance with the above described principle. The catalytic plates are extracted from the drawers and transported to the hot laboratory in separate envelopes, each being identified by its PAR number, batch number and running number. A report is produced which clearly identifies the operator, the conditions of the test, the plates and the results.

The complete procedure is illustrated in a detailed flow chart which covers all possible outcomes of the tests with the relevant actions to be taken in each case.

The TIIE was originally fed with two gas bottles containing respectively the test gas and the air which is used to flush the test section between two tests. Because the reception tests performed before delivery showed that flushing was not compulsory to check reliably the activity of the catalyst, the corresponding steps were removed from the procedure, which simplifies significantly the operation mode and saves time.

At last, as all catalytic plates are systematically tested before delivery, the acceptance tests which are performed just after installation address only 5% of the recombiners.

5. Recommendations for further development

The installation of PARs in the Belgian plants and especially in Doel1 is a world first. Nevertheless, particular issues should be further investigated to optimise the recombiners.

Concerning the recombination process itself, it has been confirmed that carbon monoxide is not a poison for the catalyst but that it is effectively recombined. However, quantitative performance tests need to be performed to measure the recombination rate at different prototypical conditions.

The qualification programme against potential poisoning agents should be extended to cyanide and to other fission products. However, in this respect, it must be kept in mind that an exhaustive list of all potential poisons cannot be produced because of the uncertainties inherently related to the severe accidents.

Although the housing adequately protects the catalytic plates against falling water, operational capability under spray conditions should also be extensively examined.

Design modifications can improve the accessibility to the catalytic plates for the in-service inspection. In this respect, the on-field experience of the Utility is the most reliable source of information.

Moreover, more flexibility should be provided in order to adapt the direction of the outlet flow to local geometric constraints.

The catalytic recombiners work at higher rates and in a much broader range of conditions than the thermal recombiners designed for the LOCAs. Hence the Belgian Utility intends to remove the thermal devices and to rely only on the PARs for all accident conditions once all plants will be equipped; this will also involve a modification of the technical specifications.

6. Conclusions

The installation of Passive Autocatalytic Recombiners is a challenging task in an existing plant because of the numerous geometric and operational constraints. Nevertheless, the objective has been achieved in Doel1 on a tight time schedule thanks to the efforts of the different partners. The next plants to be equipped are Doel3 in March 96, Doel2 and Tihange2 in May 96, Tihange1 in October 96, and finally Tihange3 and Doel4 in 97.

As a conclusion, it must be reminded that an efficient hydrogen control prevents any risk of deflagration or detonation and hence precludes the associated loads to the containment. Moreover, it brings more possibilities in managing an accident, suppressing the restrictions on the use of containment heat removal systems. Those significant advantages can now be obtained by installing Passive Autocatalytic Recombiners which are recognised as safety oriented components with no negative impact on the course of an accident.

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TABLE 1. MAIN FEATURES OF THE SIEMENS RECOMBINERS

Model	Number of plates -	Catalytic Surface (m ²)	Length (mm)	Depth (mm)	Height (mm)	Weight (kg)
FR-1500	150	11.76	1550	326	1400	109
FR-960	96	3.22	1010	166	1000	39
FR-320	32	1.07	370	166	1000	24

TABLE 2A. MAIN DATA

	Doel3	Doel1
Containment (m ³)	60600	42830
Zirconium (kg)	20426	10201
S5% (m ²)	250	176

TABLE 2B. DISTRIBUTED CATALYTIC SURFACE

Model	Sunit (m ²)	Number (-)	Sinstalled (m ²)	Seffective (m ²)
FR-1500	11.76	18	211.7	211.7
FR-960	3.22	5	16.1	10.7
FR-320	1.07	1	1.0	0.7
TOTAL		24	228.8	223.1

TABLE 2C. CHECK ON TOTAL EFFECTIVE SURFACE

$$S_{tot} \geq 1.2 * S_{5\%}$$

$$223.1 \text{ m}^2 > 1.2 * 176 \text{ m}^2 = 211.2 \text{ m}^2$$

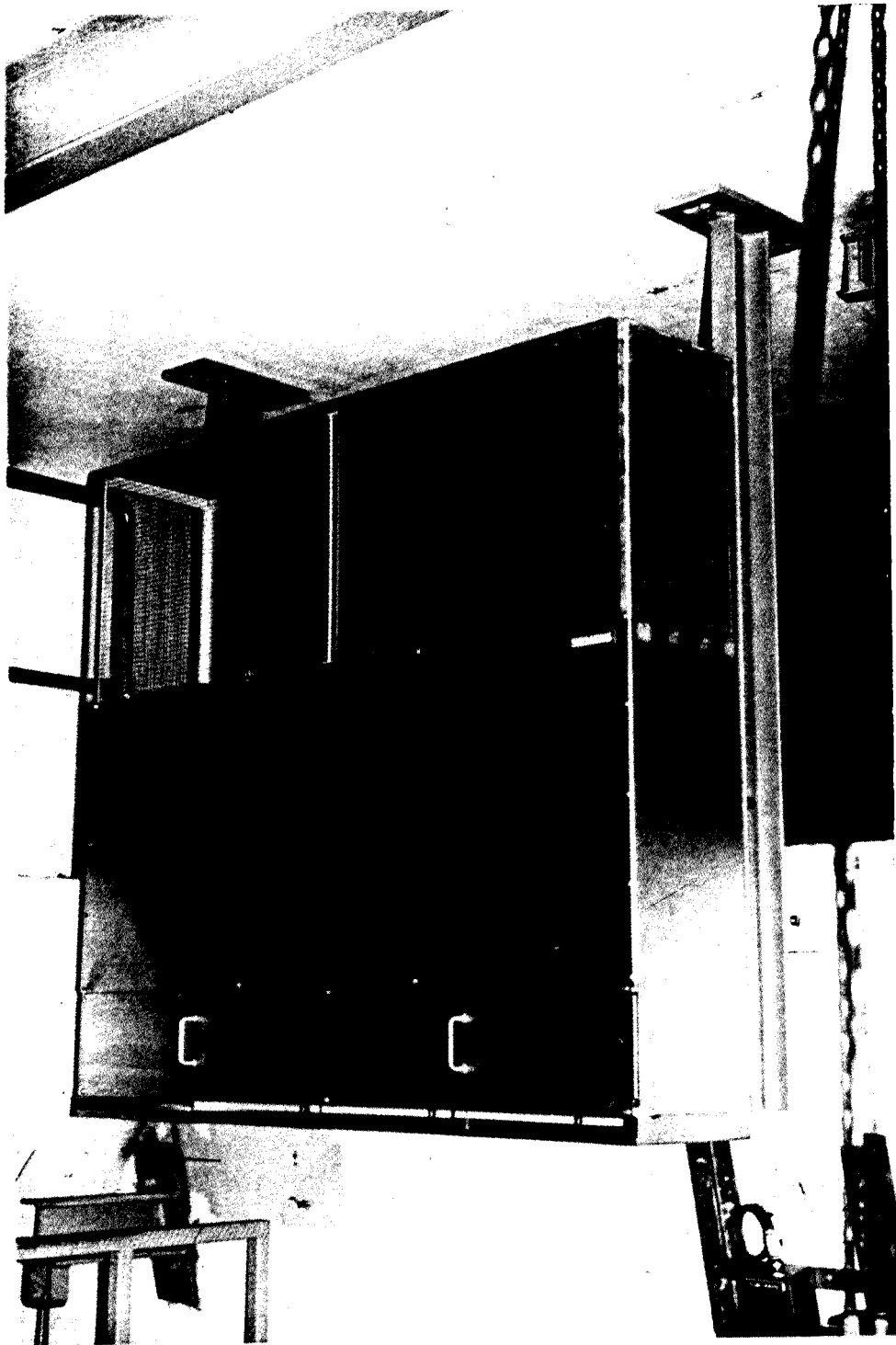
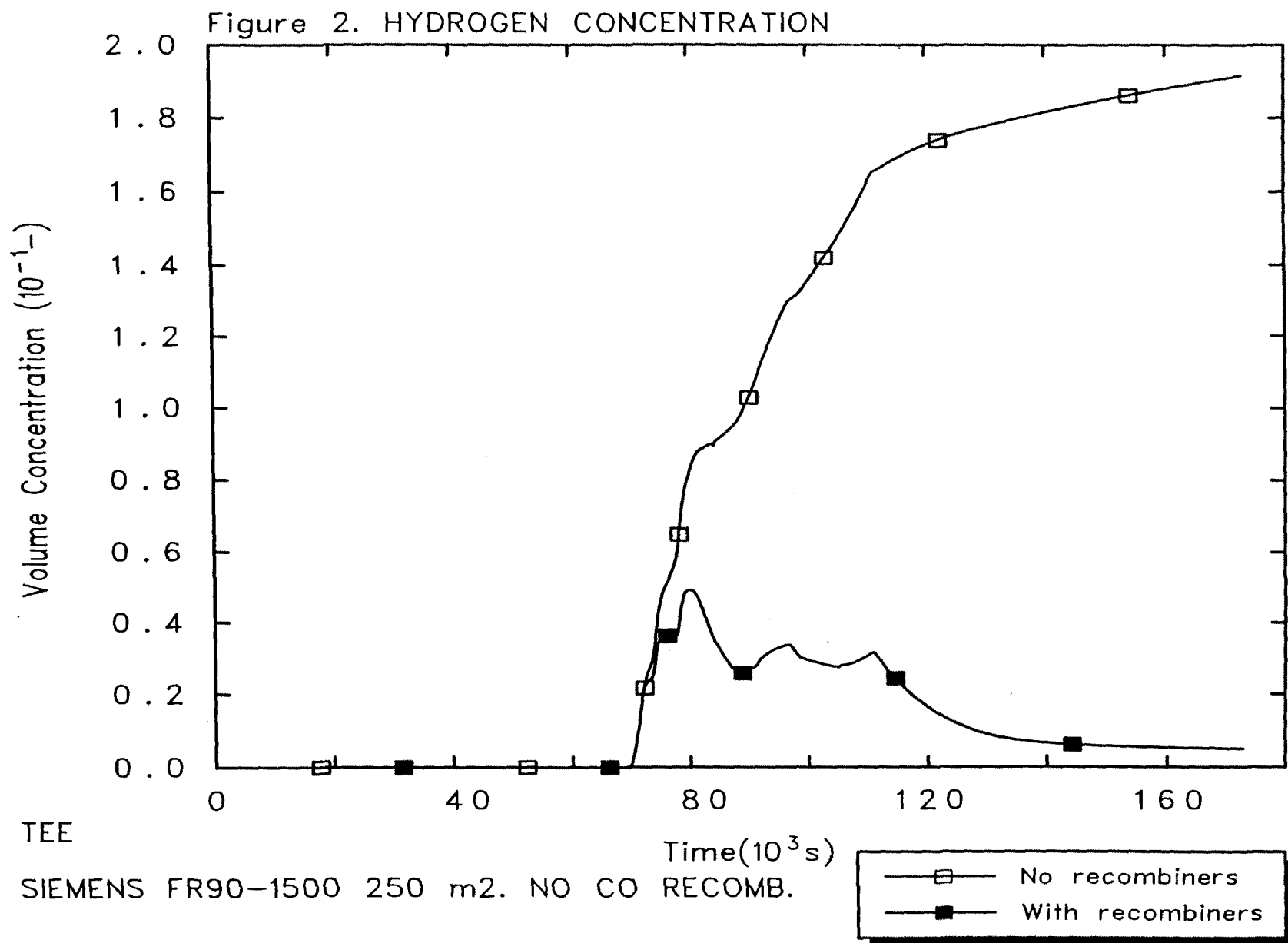


Figure 1. PASSIVE AUTOCATALYTIC RECOMBINER



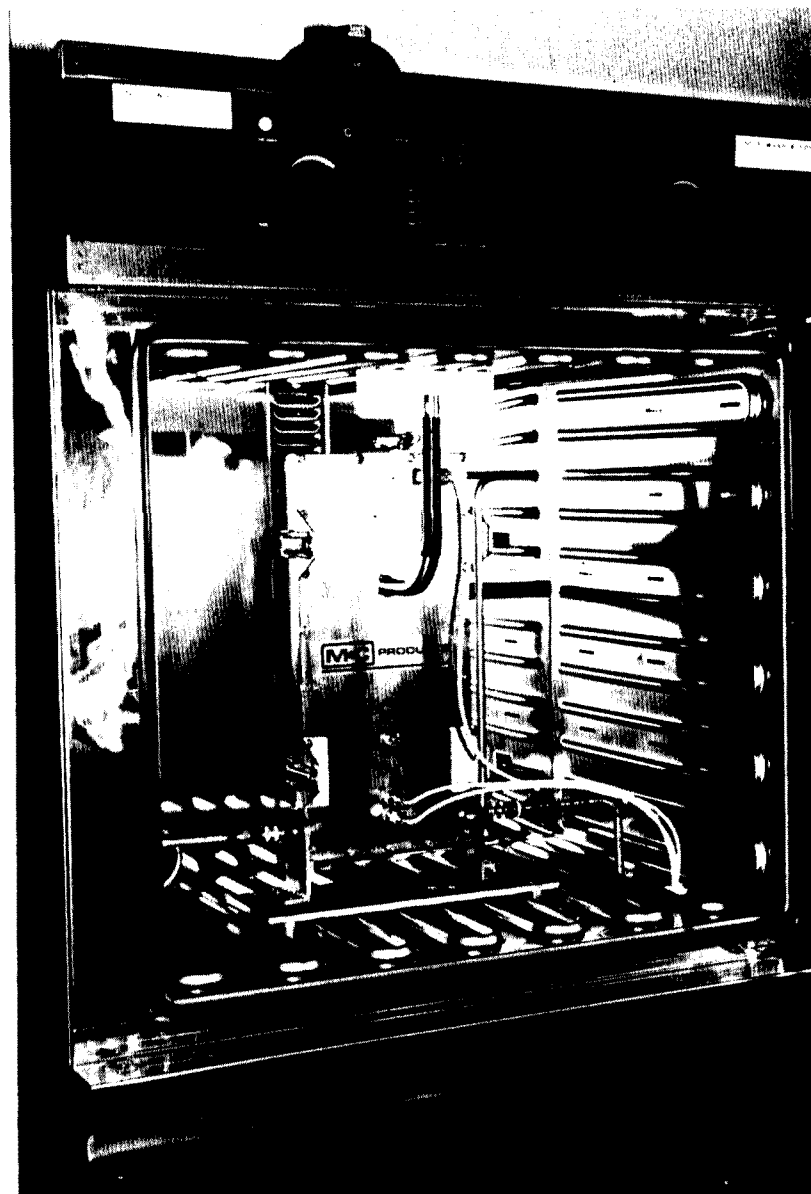
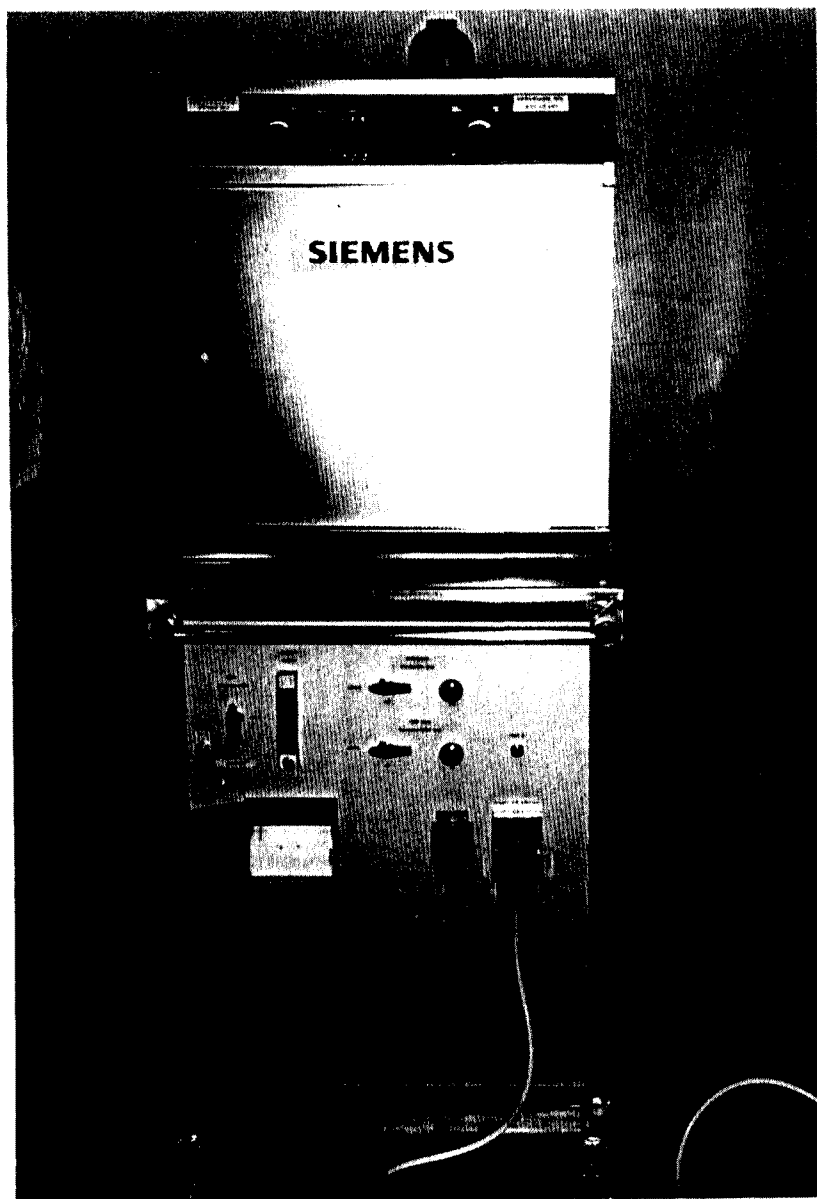


Figure 3. TRANSPORTABLE IN SERVICE INSPECTION EQUIPMENT

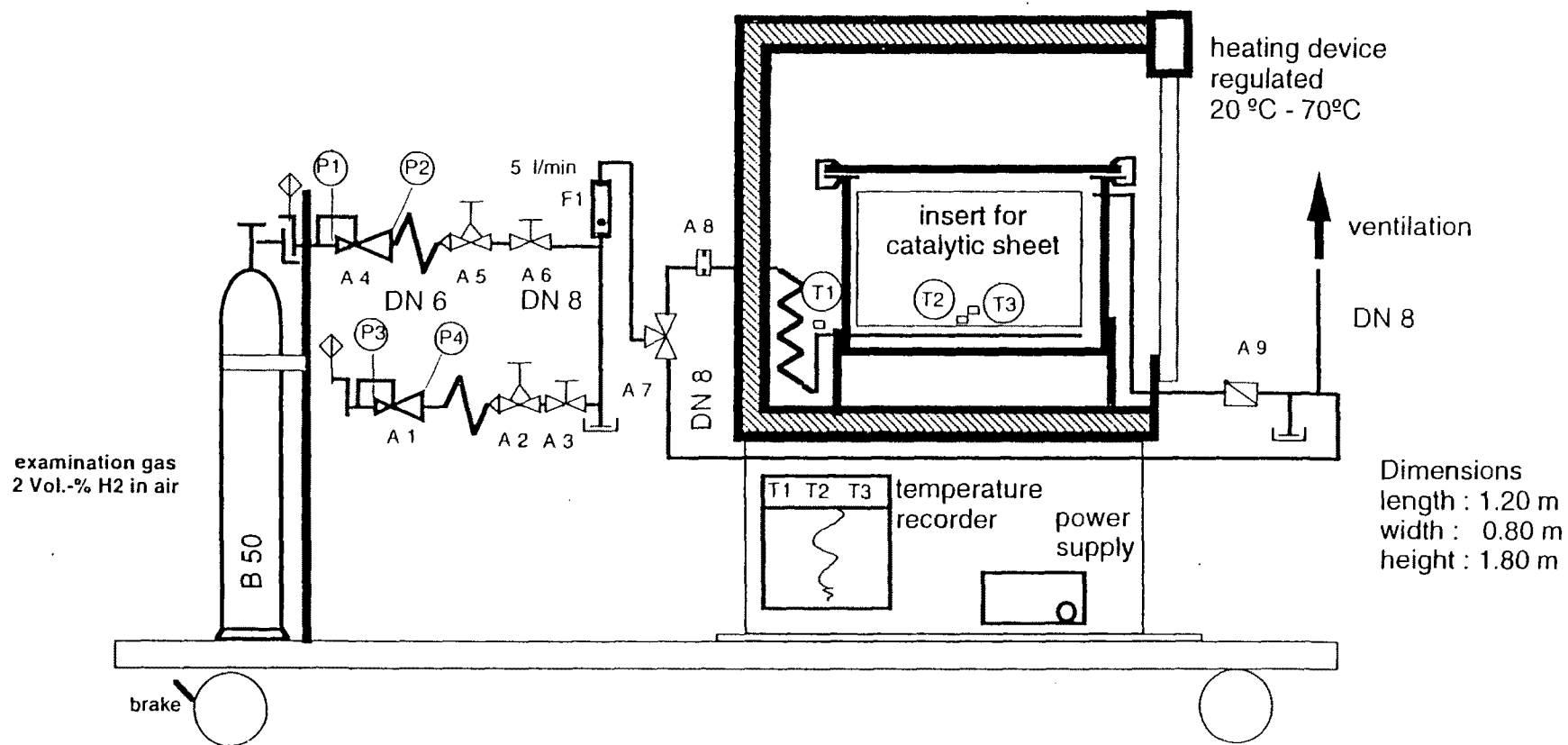


Figure 4. THE. FLOW DIAGRAM

SESSION IV

TECHNOLOGY

Chairman: R.J. Fluke (Ontario Hydro, Canada)

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4.1 HYDROGEN RECOMBINER DEVELOPMENT AT AECL

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Abstract

Catalytic recombiners have been developed at AECL for the purpose of hydrogen removal in post-accident nuclear containment buildings. The recombiners are based on a particular catalyst designed by AECL which has extraordinary resistance to fouling from water and water vapour and a large thermodynamic range of operation. The catalysts were developed, originally, for the purpose of heavy water manufacturing by way of a catalytic exchange process. Application of these catalyst materials in recombiners for containment applications began in the late 1980's. The first application was a passive recombiner, qualified for use in control of radiolytic hydrogen in the headspace of a pool-type experimental reactor of AECL design in 1988. The passive, or natural convection recombiner concept has continued development to commercial stage for application in power reactor containments. This paper reviews the AECL recombiner development, describes the current model and shows results from tests of full-scale recombiners in the Large Scale Vented Combustion Test Facility at AECL-WL.

The AECL recombiner is designed for compactness and ease of engineering into containment. The design is a simple, open-ended rectangular enclosure with catalyst elements arranged inside to promote optimum convective flow driven by heat of recombination at the catalyst surface. Self start, as evidenced by catalyst heating and initiation of flow, is achieved in less than 1% hydrogen, with available oxygen, at room temperature and 100% relative humidity. This low temperature start-up in condensing atmospheres is viewed as the most challenging condition for wet-proofing effectiveness. Cold start-up is a vital performance requirement in containments, such as CANDU®, where engineered air-cooling systems are operating and where long-term hydrogen control is required, after containment atmospheres have cooled. Once started, the removal capacity scales linearly with the inlet cross-section area and the partial pressure of hydrogen. The recombiner also reacts carbon monoxide, in the presence of hydrogen, at approximately the same rate as the hydrogen. The catalyst materials and wet-proofing are unaffected by radiation or high temperatures. Large scale tests confirm self-start behavior and demonstrate strong mixing, irrespective of recombiner placement.

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Introduction

Hydrogen gas generated in a reactor accident poses a threat to structures and essential equipment if allowed to accumulate to flammable concentrations and ignite.

Catalytic oxidation is a way of removing hydrogen at concentrations below flammability limits and has been demonstrated to be effective for containment applications [1-7]. Devices for catalytic hydrogen removal in containment are at the stage of commercial availability and are being installed in existing stations and included in new designs to improve safety margins for hydrogen [8,9]. The principle of operation can be described, briefly, as follows. Hydrogen molecules coming into contact with catalyst surfaces are reacted with oxygen in the air. The heat of reaction at the catalyst surface provides a driving force for buoyancy-induced flow over the catalyst elements, without mechanical assistance or outside power. The action is thus self-starting in response to the presence of hydrogen (with available oxygen) and continues until the hydrogen (or oxygen) in the vicinity has been consumed. Although the capacity depends on several factors (hydrogen concentration, the catalyst activity and aerodynamics of the catalyst arrangement), it is considered feasible by this method to process the entire volume of containment air on the order of hours.

Historically, an obstacle to total passive operation was that catalyst materials most suited to hydrogen were prone to deactivation by ambient water vapour and required outside heat or high ambient temperatures in order to start. However, catalyst materials developed at Atomic Energy of Canada Limited (AECL) have high activity for hydrogen oxidation and are active at ambient temperatures, wet or dry [10,11].

This paper describes the development and testing of catalytic recombiners, based on the AECL catalyst, for hydrogen removal in containment.

AECL Wetproofed Catalysts

Engineering of wetproofed catalysts for the hydrogen-oxygen reaction has been underway at AECL for over 20 years as part of a continuing program of research into methods of producing heavy water using catalytic isotope exchange processes. The program is motivated by the need for new methods of producing heavy water - a key element of CANDU technology. The liquid-phase catalytic exchange process (LPCE) is the most promising candidate for safe and low-cost heavy water production in the future. Engineering of advanced catalysts for the LPCE process is a continuing high-priority activity at AECL and is at the pilot plant phase of development.

In the LPCE process, the catalyst must be near the mass-transfer surface, so wetproofing is required to prevent contact between the liquid and the catalyst surface. Optimizing wet-proofing characteristics is thus a priority. Further, the transfer of deuterium from hydrogen gas to liquid water is thermodynamically favoured at low temperatures, so optimizing low-temperature performance has also been a priority. The catalyst materials developed in this program are unsurpassed in wet-proofing and low temperature performance.

In the 1980's, many applications were identified where catalyst resistance to water vapour was desirable (see Table 1). Wetproofed recombiners that operate in cool humid conditions have a

significant advantage over conventional recombiners, which must be heated to prevent catalyst deactivation. For example, trickle-bed recombiners, which use liquid water to remove the heat of recombination from the catalyst, were developed for recombining near-stoichiometric mixtures of hydrogen and oxygen [11]. Gas-phase wetproofed recombiners were also developed for many diverse applications, such as tritium sampling [12] and removal of radiolytic hydrogen in canisters of wet radioactive material [13].

The catalyst used in the recombiners described in this work is a proprietary AECL formulation developed specifically for application in nuclear containments [2,3]. The catalyst has a high catalytic activity for hydrogen oxidation, is not deactivated by water vapour or steam, and is specially formulated for operation over a very wide range of temperatures. It operates at temperatures up to 1000 K without loss of wet-proofing or catalytic activity and is unaffected by high radiation exposures. It has shown resistance to poisoning by anticipated containment gases.

Table 1

History of AECL Wetproof Catalyst Engineering

1972	First wetproofed catalyst designs using coated catalyst pellets.
1983	Radiation-resistant, wetproofed catalysts.
1984	High-temperature wetproofing formulas; bonding agents developed for carbon, stainless steel and ceramic support materials.
1985	Large-scale trickle bed recombiners.
1986	Hydrogen control in canisters of wet, radioactive materials.
1987	Catalysts for tritium sampling.
1988	Natural convection recombiner qualified for hydrogen control in head space of SES-10 an experimental reactor of AECL design.
1989	Demonstration testing of a model catalytic combustor (forced convection recombiner) for ventilation systems of power reactor containments.
1990	Demonstration testing of autocatalytic (natural convection) recombiners for power reactor containments.
1991	Pre-engineering test program for autocatalytic recombiner - sponsored by the CANDU Owners Group.
1994	Full-scale prototype demonstration and qualification testing.
1996	Qualification testing of commercial model recombiners in the Large-Scale Vented Combustion Test Facility at AECL-WL.

AECL Recombiner Description

Design Considerations. Based on prototype tests, plant analysis and feedback from designers and station owners, the following considerations guided the commercial design.

- cost of manufacturing
- ease of scaling the required capacity to containment subvolumes
- ease and versatility of interfacing to containment structures
- ease of installation
- ease of access to catalyst elements for testing
- protection from environment (sprays, missiles, etc.).
- function and performance
- qualification for service in severe accident environments

Design Description The commercial model of the AECL recombiter consists of open-ended rectangular catalyst modules mounted in a rigid steel frame with an attached cover (see Fig. 1). Each module contains flat, rectangular, catalyst elements arranged to allow optimum flow through the enclosure. The frame is made from rectangular steel tubing which provides a secure location for the catalyst module and an interface to containment structures. The optional cover provides physical protection to the internal elements from sprays or missiles. To optimize space usage, the frames are custom-designed and prefabricated for the particular proposed locations.

Installation is envisioned to proceed in three steps: first, the frames are installed in the chosen locations; then, the catalyst modules are set into the frames (for ease of installation, the catalyst modules are all the same size, weigh less than 25 kg and have no directional requirements - no front or back). Finally, the covers are installed on the catalyst holders - either a hooded-cover or a simple grill-cover - to direct flow or shelter the modules in the desired way, depending on location.

Catalyst elements are extracted for routine testing by releasing the hinged cover and lifting individual catalyst elements from the holders. The catalyst elements will be tested in a simple device which produces a positive indication of the required catalyst activity.

Demonstration Tests 1993-1995

Function and performance of the AECL recombiter were demonstrated in the 6.6 m³ and 10.7 m³ Containment Test Facility vessels at AECL Whiteshell Laboratories using a 1/10-scale test model and a full-scale prototype recombiter [14]. Self-starting limits, operating limits, removal capacity, scaling parameters and mixing behaviour in the vicinity of an operating recombiter were demonstrated along with resistance to poisoning, fouling and radiation. The essential results of this phase of testing are summarized in Tables 2 and 3.

Full-Scale Tests in the Large Scale Vented Combustion Test Facility

The AECL recombiners currently being tested in the newly-commissioned, Large-Scale Vented Combustion Test Facility (LSVCTF) at AECL-WL. The LSVCTF offers a high degree of control and accurate measurement of gas compositions as well as a larger volume than previous tests in the CTF.

Preliminary results from the first test series are given here. The aims of the first test series were:

- 1) to confirm the previously demonstrated self-start limits in 1% H₂ at room temperature in condensing atmospheres,
- 2) to confirm the previously established hydrogen removal capacity and single pass efficiency as a function of hydrogen concentration,
- 3) to measure mixing produced by the recombiner and the sensitivity to placement of the recombiner within the enclosure,
- 4) to provide validation data for the recombiner model in GOTHIC, and
- 5) to study effects of gratings and flow directing devices on recombiner performance.

Facility Description. The LSVCTF is a 120 m³ (10-m long, 4-m wide and 3-m high) structural-steel enclosure, instrumented, insulated and temperature-controlled for operation from ambient temperature up to 140°C (see Fig. 2). The design pressure is 300 kPa (g), with a dynamic load factor of 2. The facility has systems for controlled addition of hydrogen, steam and inert gases. All aspects of facility operation are carried out from a remote location 400 m from the test chamber. The composition of gases is measured at several (up to 24) different locations using an industrial process mass spectrometer with a high speed rotary sampling valve. Hydrogen, oxygen, steam and other light gases are measured simultaneously within about 4 s of real time, with an accuracy of 0.1 vol.%. The time required between sample locations is between 5 s and 30 s, depending on the number of gases analyzed and the accuracy required. Further details of the facility are provided in another paper at this workshop [15].

Test Description. For these tests the recombiner module was fitted with a pneumatically-operated damper to remotely open and close the inlet and outlet of the module. This was to enable the experimenter to isolate the functional parts (i.e., catalyst elements) inside the recombiner from the surroundings (i.e., turn it 'off') while the initial conditions were being established. Once the target conditions were created and verified by the gas analyser output, the recombiner was turned 'on' by opening the dampers - providing a definitive starting point for the test.

Two sizes of recombiner were tested, the test model from the previous CTF tests and the commercial model. The test model was 32 cm x 50 cm high with a flow area of 0.1 m². This is one half the area of the commercial model but the heights, spacings and types of catalysts are identical to the commercial model. The recombiner module was placed at one of two locations; the geometric centre of the enclosure or at one end of the enclosure (1.5 m from one end wall and 8.5 m from the other end wall). Fig. 3 shows a sketch of the facility configured for recombiner tests. Gas compositions were measured at the inlet and outlet to the recombiner and in the bulk gas at a point 1 m from the far wall at mid-height of the enclosure.

During the addition of gases, the dampers on the recombiner were closed to allow the desired mixture to be achieved before recombination started. The gases were added with the mixing fans running to allow rapid mixing. Steam was added first, then hydrogen. The steam was added to above the saturation pressure, so that there was initially 100% humidity plus wet fog in the combustion chamber before the hydrogen was added. The mixing fans were turned off for 2 minutes to allow the gases to become quiescent, then the dampers were opened to start the test. Tests were done at $\sim 25^{\circ}\text{C}$, at ambient pressure.

Test Results - 0.1 m^2 Test Model

The first test demonstrated the self-start of the recombiner at 1% hydrogen, with the recombiner in the central location. Further tests were done at 2%, 4%, and 6% hydrogen in both the central and far wall positions. One additional test was performed at 4% hydrogen, with the recombiner centrally located, and with a 45° exhaust duct at the outlet and gratings on both the inlet and outlet. These obstructions were intended to represent the same geometry as the commercial recombiner options.

Self Start and Mixing. The mass spectrometer output for the 1% H_2 test is shown in Fig. 4. At time = 00:00:00, the recombiner dampers were opened and the catalyst elements became exposed to the hydrogen-steam-air mixture. The record of addition of gases to the chamber is shown on the negative side of the time axis. Immediate startup of recombination is evident from the abrupt drop in hydrogen concentration at the outlet of the recombiner. The extent of mixing of gases in the enclosure was good, as indicated by the fidelity of bulk gas and inlet samples.

Placement and Obstructions. Figures 5 and 6 show the results in 4% H_2 from two different locations of recombiner (central and far wall). It can be seen that the location of the recombiner did not have a strong effect on the overall recombination rate or the mixing of gases in the enclosure except for small differences in time delay due to distance. The gas composition at the bulk-gas measuring point with the far-wall placement required approximately 5 minutes longer to reach 1% H_2 than with the central placement. Figure 7 shows the results, in 4% H_2 with obstructions installed at the inlet and outlet to the recombiner. The obstructions also did not have a strong effect on recombiner performance.

The recombiner efficiency is approximately 0.6, as determined from inlet and outlet hydrogen concentrations. This recombination efficiency seems to be independent of hydrogen concentration, but is usually higher at the very beginning of a test. There is a trade-off between the reaction rate and the residence time. Low hydrogen concentrations produce a lower operating temperature, lower buoyant driving force and thus longer residence times. At higher concentrations, the temperature is higher and the catalyst is more active but the residence time is shorter due to higher flow velocities. At a particular concentration, obstructions to the flow have an effect of increasing the efficiency, but the net capacity is not improved.

Test Results - Commercial Model (0.2 m²) Recombiner

The commercial recombiner was tested in the LSVCTF under conditions self-consistent with tests on the test model. The commercial model has two times the inlet area (i.e., two times the capacity) of the test model, has a lighter weight housing, gratings to protect the inlet and a partial cover to protect from sprays. But the commercial unit lacks the dampers to isolate it from its surroundings for test purposes.

Selected results of large scale tests of the commercial model are shown in Figures 8, 9 and 10. The cold, wet self-start behaviour at 1% hydrogen concentration is consistent with all previous tests (Fig. 8). It is noted, of course, that initial conditions are not as precisely defined as when the dampers were used with the test model. The measured capacity of the commercial model is shown in Fig. 10 as a function of hydrogen concentration. A full qualification test program for the commercial unit in the LSVCTF is continuing.

Conclusions

Catalytic recombiners have emerged as a credible option for hydrogen mitigation in nuclear containments. Wet-proofed catalysts, developed at AECL, show superior self-start properties under cold-wet conditions and good resistance to poisoning and fouling. Recombiners have been designed using these catalysts and demonstrated under conditions relevant to post-containment atmospheres. The commercial recombiner design features a light-weight catalyst module in rigid frame, custom-designed for ease of sizing and installation. Large-scale tests in the LSVCTF confirm self-start capability and demonstrate strong mixing irrespective of recombiner placement in the enclosure.

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TABLE 2

**Summary of Experiments with Test Model AECL Passive Recombiner
in the 6.6 m³ and 10.7 m³ Containment Test Facility Vessels**

Type of Test	# of Tests	Result
configuration and geometry of internals - axial channels - corrugated plates - flat plates - spacing/height ratio - effect of chimney	33	•parallel flat plates with height to spacing ratio of 10 is the simplest configuration for optimum performance (capacity) •straight chimney increases capacity by 30% - 50% per metre of height
capacity and the effect of initial thermodynamic conditions (pressure, temperature, humidity and concentration of reactants)	11	•capacity is unaffected by humidity •capacity increases in proportion to initial pressure in the range of 1-2 bar •capacity is insensitive to initial temperature between 20 and 120°C •capacity increases with increasing concentration of the limiting reactive constituent (H₂ or O₂)
self-start limits	25	•self-start is spontaneous at <1% H₂ (with available O₂) and <1% O₂ (with available H₂) at 25°C, 100% R.H. •self-start limits are unaffected by 90% H₂O, CO₂ or N₂ diluents
scaling 1/10-scale and full-scale tests	4	•capacity increases in proportion to the inlet cross-section area
mixing behaviour (10.7 m ³ cylinder)	6	•stratified atmospheres over a 5 metre height are homogenized within 3 minutes by natural convection from an operating recombiner
effect of water spray	4	•direct spray into an uncovered recombiner cools the catalyst plates and reduces performance. When spray stops, the catalyst reheats and resumes normal performance
effect of steam spray	4	•direct steam spray increases hydrogen removal rate due to forced convection
effect of radiation	2	•capacity and self-start limits are unaffected by 480 kGy γ-radiation to catalyst materials

(continued...)

TABLE 2 (concluded)

effect of deliberate exposure to H ₂ burn	3	•catalyst performance unaffected by 10% and 14% H₂ burns
operating temperature limit	4	•performance unaffected after operation at 700°C in 20% H₂/6% O₂
resistance to poisoning and fouling		
- boric acid	1	• after soaking in saturated boric acid solution: - self start limits unaffected - some reduction of capacity due to precipitate coating
- cable fires	2	•after <u>heavy</u> sooting from a cable/kerosene fire: - no effect on self-start - small reduction of capacity
- carbon monoxide	6	•carbon monoxide is oxidized to carbon dioxide at approximately the same rate as hydrogen oxidation •hydrogen removal capacity is unaffected by carbon monoxide •self-start limit for hydrogen recombination increases to 2% H₂ with prior exposure to 2% carbon monoxide
- methyl iodide	1	•10⁻⁷ M CH₃I in the gas phase had no effect on the capacity. Self-start limit increases to 3% H₂
- formaldehyde	1	•no effect
- benzene	1	•no effect
ageing	2	•catalyst plates exposed to industrial atmosphere for 4 years exhibited no change in performance characteristics

TABLE 3

**Summary of Experiments with Full-Scale Prototype Recombiner
in the Containment Test Facility**

Type of Test	# of Tests	Result
Measurements of Capacity of H₂ Removal - hydrogen concentration - initial pressure - initial temperature - humidity - chimney height	12	• capacity increases linearly with hydrogen concentration • capacity increases linearly with initial pressure • capacity is unaffected by initial temperature or humidity • capacity increases ~30% per metre of chimney height
Measurements of Capacity of CO Removal	4	• the molar rate of CO oxidation is approximately the same as H₂ oxidation
Adverse Environments - cold, wet (25°C, 100% RH) - CO (1%, 2%, 4%) - I ₂ (10 ⁻⁷ M, 10 ⁻⁸ M) - paint fumes (2 part, self-priming epoxy for nuclear containments, with Valspor thinner)	4 5 2 2	• spontaneous self-start is observed at 1% H₂ under all environments tested • CO at 2% and 4% levels produces an increase in H₂ oxidation. Effect on self-start same as small-scale tests • molecular iodine at 10⁻⁷M did not affect self-start limits or capacity • fumes from 0.3 m²/m³ freshly painted surfaces of epoxy paint produced no effect on self-start limits or capacity.

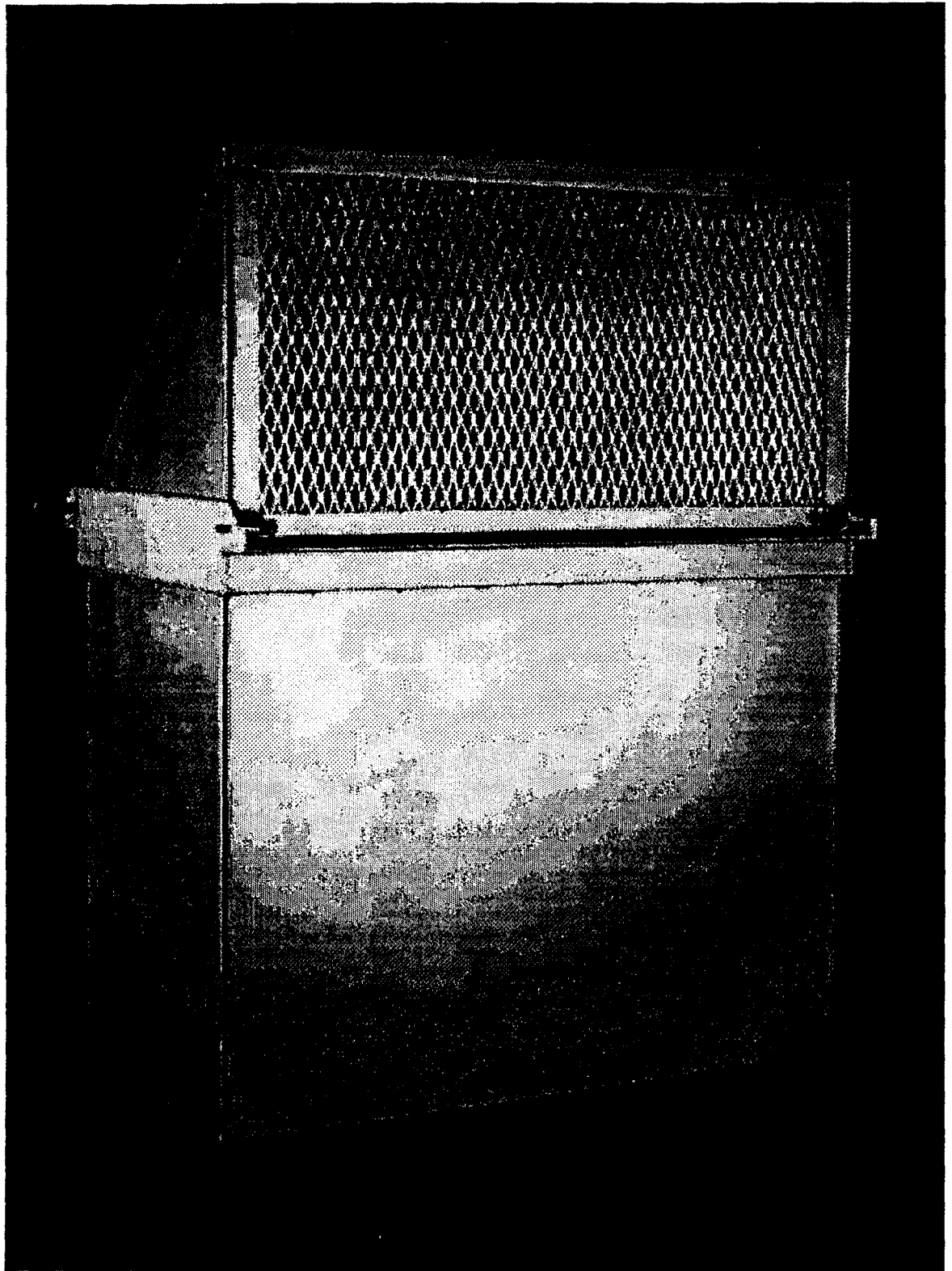


Figure 1. Picture of the Commercial Model Recombiner.

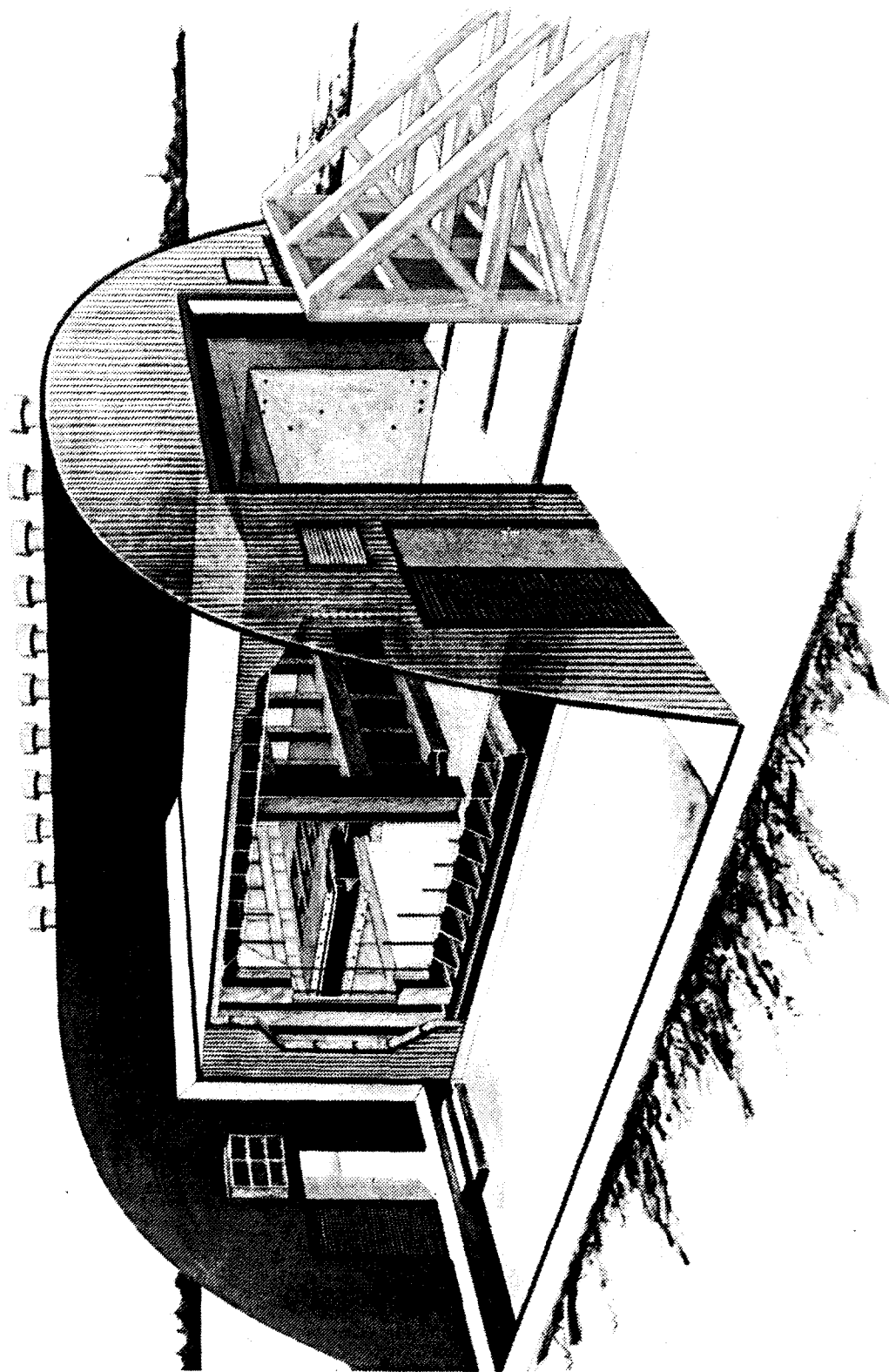


Figure 2. Schematic of the Large-Scale Vented Combustion Facility.

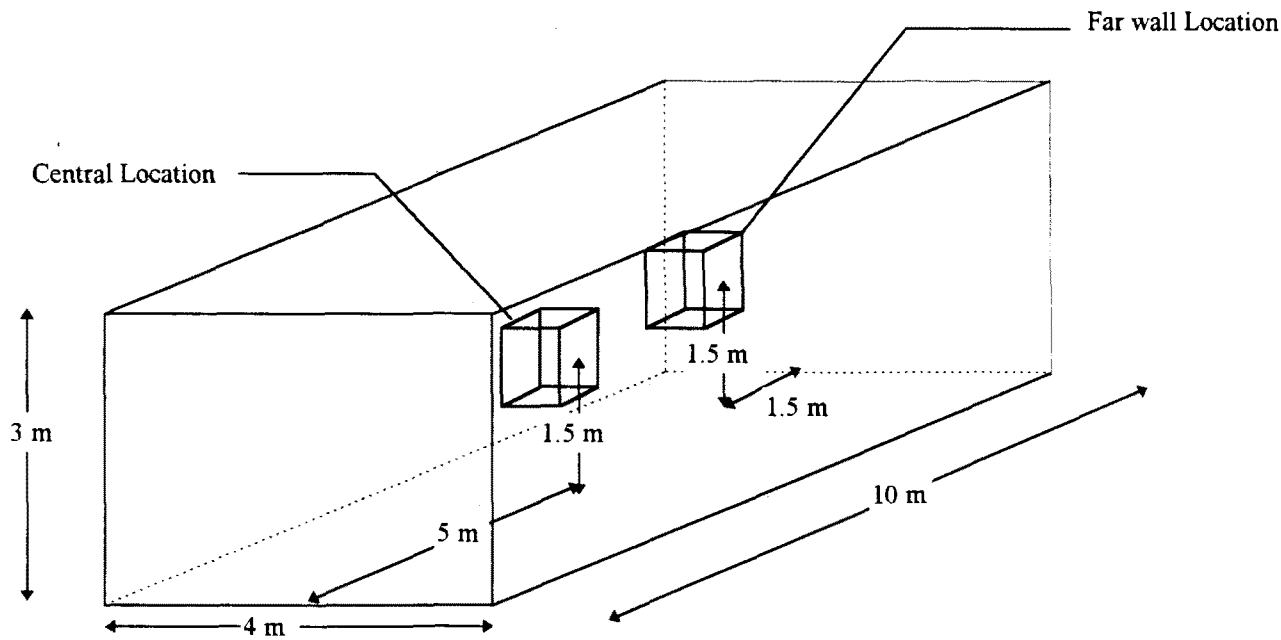


Figure 3. Schematic of Combustion Chamber with Recombiner Locations.

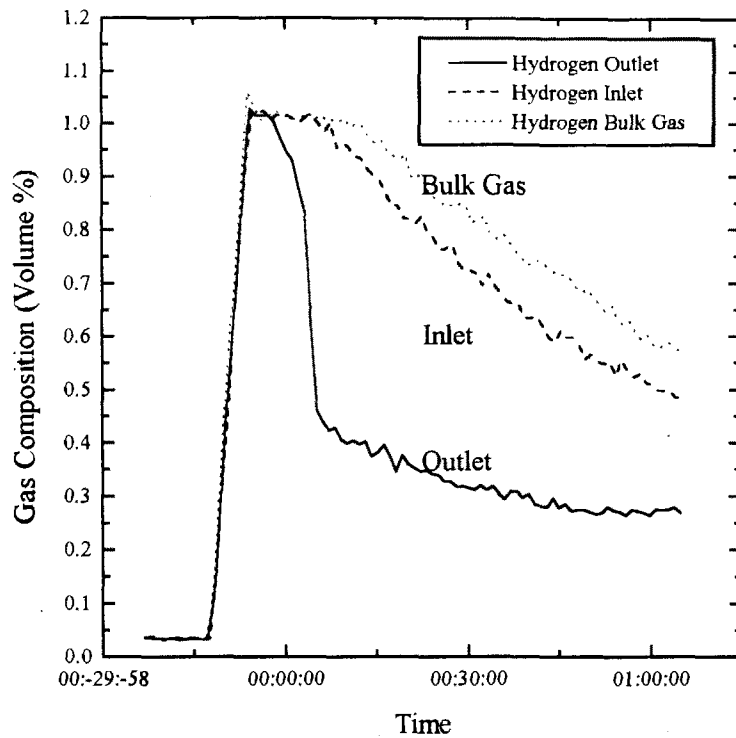


Figure 4. Gas Analysis Results for Test Model in 1% Hydrogen, 5% steam, at 25°C. Recombiner at Central Location.

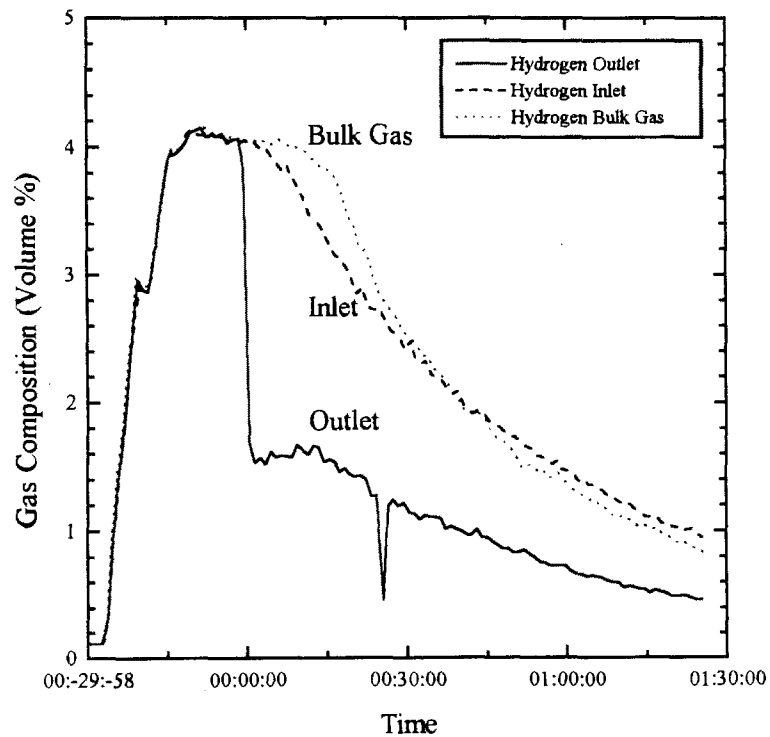


Figure 5. Gas Analysis Results for Test Model in 4% Hydrogen, 5% steam, at 25°C. Recombiner at Central Location.

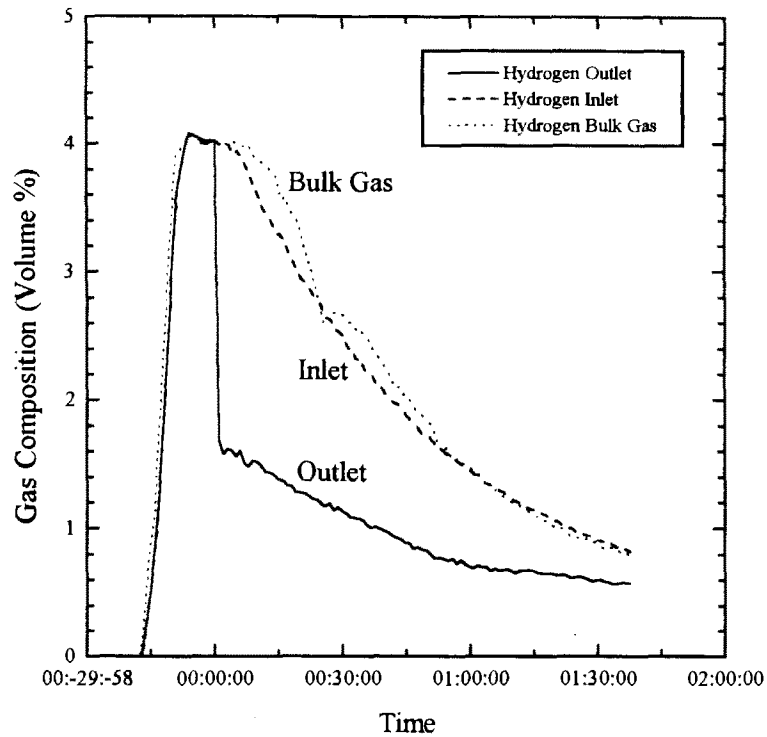


Figure 6. Gas Analysis Results for Test Model in 4% Hydrogen, 5% steam, at 25°C. Recombiner at Far Wall Location.

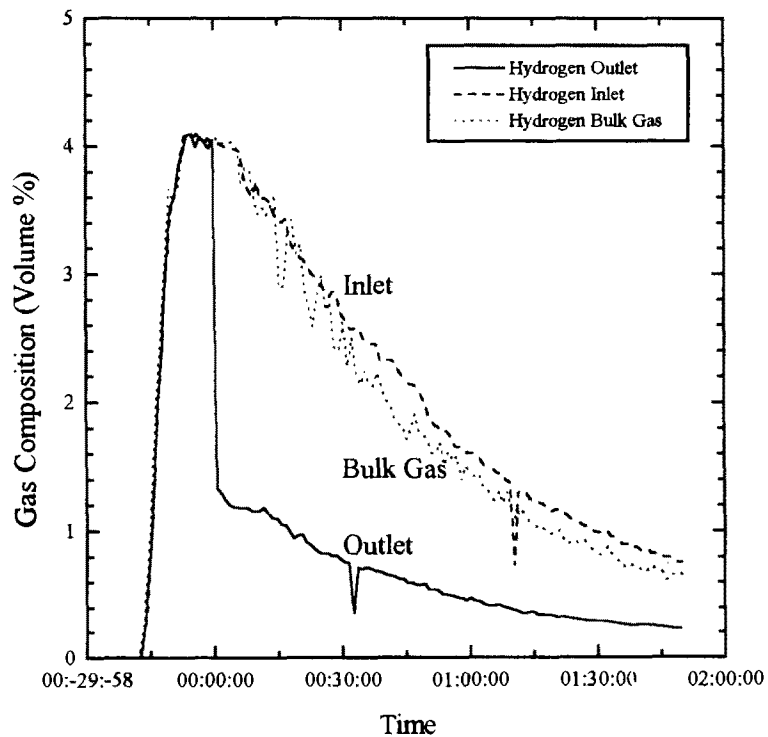


Figure 7. Gas Analysis Results for Test Model in 4% Hydrogen, 5% steam, at 25°C. Recombiner at Central Location. Exhaust Duct and Inlet Gratings Attached.

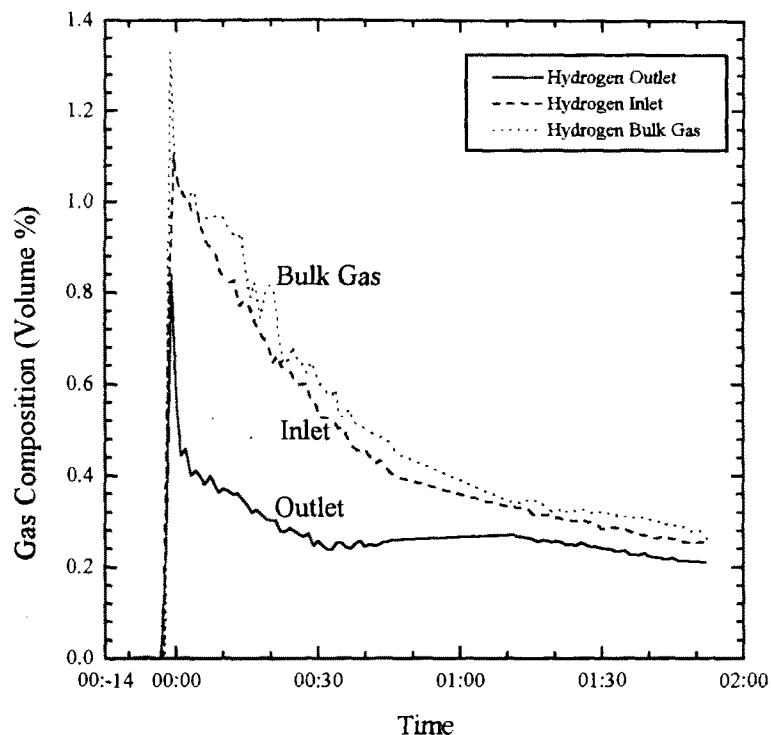


Figure 8. Gas Analysis Results for Commercial Model in 1% Hydrogen, 5% steam, at 25°C. Recombiner at Central Location.

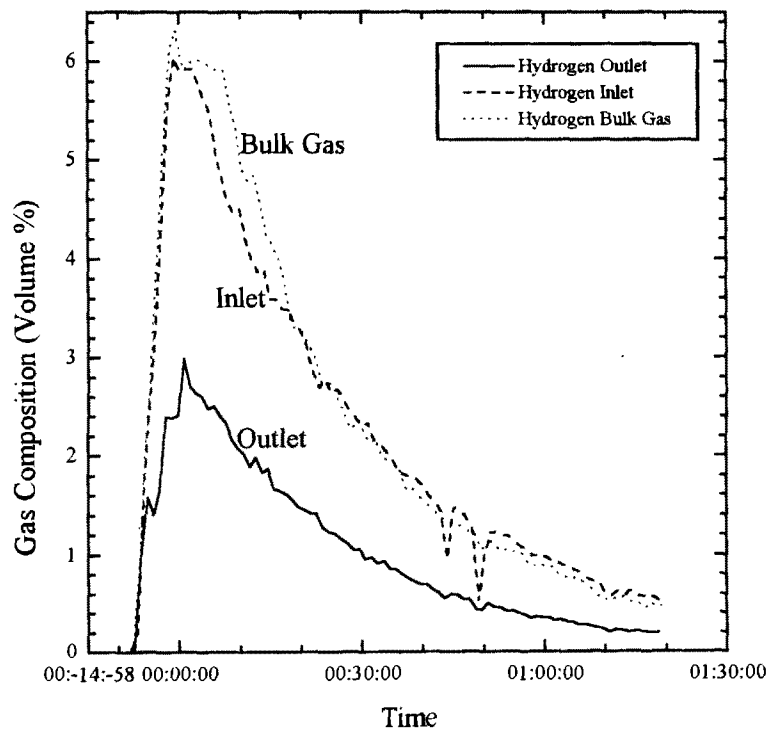


Figure 9. Gas Analysis Results for Commercial Model in 6% Hydrogen, 5% steam, at 25°C. Recombiner at Central Location.

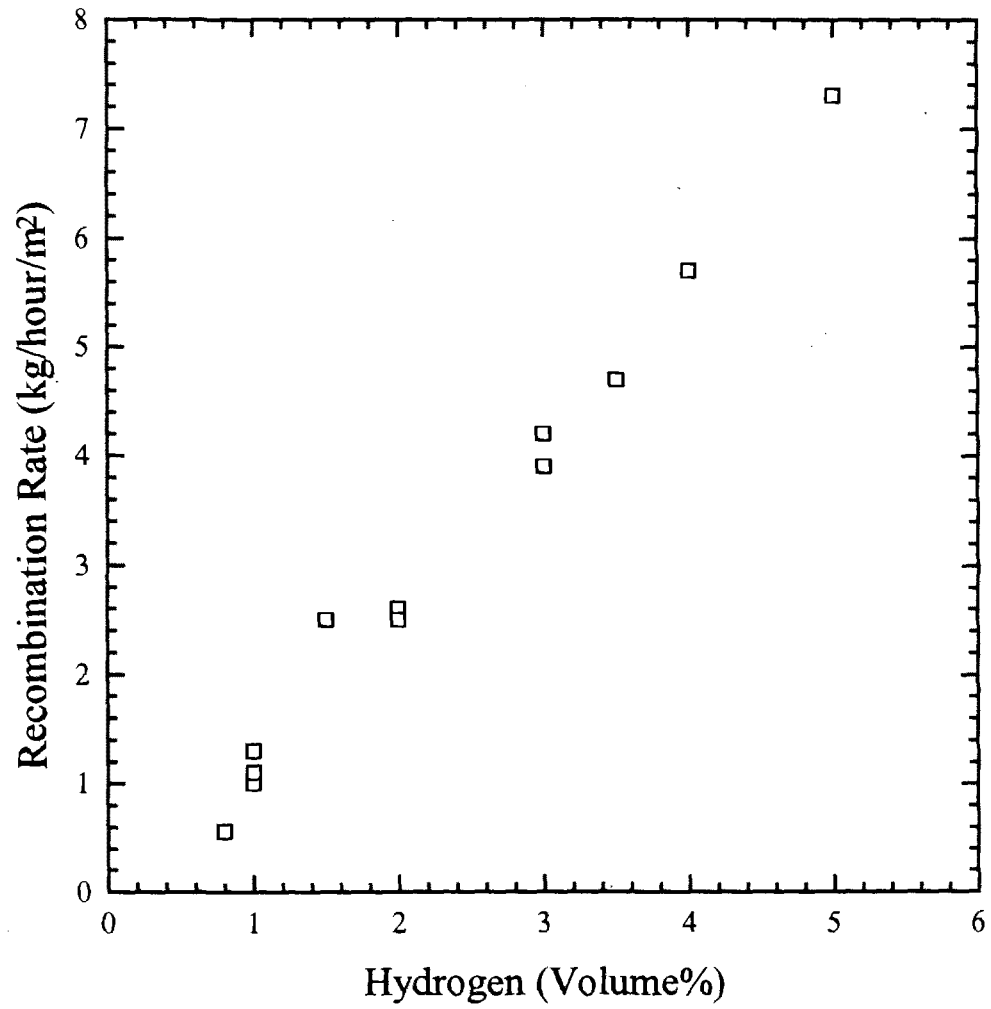


**Figure 10. Recombiner Capacity for 0.2 m²
Commercial Model Recombiner.**

4.2 THE INERTISATION OF PWR CONTAINMENTS BY INJECTION OF LIQUID CARBONDIOXIDE

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Abstract¹

Several methods are under discussion to mitigate the consequences of the release of large amounts of hydrogen which inevitably accompanies the course of any severe accident in light water cooled power reactors. The installation of ignitors is a method which is in particular difficult to defend because one is requested to support this method with difficult analytical predictions of the resulting combustion process, starting with the expected spectrum of release rates, the associated distribution process ending with the analysis of the variety of possible combustion processes. Hence, less questionable mitigation procedures should be preferably developed.

Inertisation of the containment atmosphere aims to prevent combustion either partially (exclusion of highly energetic combustion modes) or totally (exclusion of any combustion mode). Injection of liquified carbondioxide into a building is a well established method which is widely used, e.g. in inerting crude oil transporters to prevent the possibilities of explosions during the transport. Numerous other applications have been developed by fire fighting institutions and were promoted by insurance companies.

The containment code RALOC has been modified and supplemented to simulate the processes characterizing the injection of liquified carbondioxide through nozzles into a LWR containment after a preceding loss of coolant accident. Main aim of the liquid CO₂ injection is to arrive rapidly at a uniform distribution of the inert gas within the free containment atmosphere. Large driving temperature differences are formed inside the containment which promote a natural convection driven distribution process. Sufficient heat capacity is available from the post LOCA atmosphere and associated to the large masses of containment structures, which facilitate the rapid evaporation of generated CO₂ particles. This process is also modelled by the code.

Several analyses have been performed with the modified code showing the transient distribution until uniform concentration of the atmospheric components steam, air and

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carbondioxide is reached. Time intervalls necessary to achieve the required inerting status of approx. 60 vol% may easily be below 30 minutes.

The combination of inertisation with the installation of a number of catalytic recombiners remains as a supplementary option.

A limited number of experimental results has been made available by fire fighting industry to facilitate the application of the modified code for validation purposes. Comparisons between analyses and experimental results are satisfactory. However, some tests, more typical for PWR containments would be desirable to confirm the post-accident inertisation simulations.

1 Introduction

During the course of severe accidents in lightwater cooled nuclear power plants large amounts of hydrogen could be generated and released into the containment. The formation of hydrogen inevitably accompanies any core degradation process. Later, it may be amplified by a less likely core-concrete interaction during the basemat degradation. The integrity of some large dry full-pressure-containments might be challenged by hydrogen explosions if no mitigative measures are available. Internal hydrogen explosions may cause severe damage on internal structures resulting in the formation of concrete missiles bearing high damage potentials. For steel shell containments the pressure bearing steel shell would be directly exposed to the mechanical impact of such missiles. For massive prestressed or reinforced concrete shells the pressure bearing steel structures are covered by thick concrete layers that can protect them. The latter type of containments is in general challenged by global hydrogen explosions only which are less probable. But more than 40 steel shell containments exist that must be protected against consequences of possible local explosions [1].

2 Requirements for Post-Inertisation as Hydrogen Mitigation Strategy

Severe accident scenarios which are characterized by release of large amounts of hydrogen in general will be initiated by a variety of unmitigated loss of coolant accidents (LOCA). During the blowdown phase and later during the phase of possible core degradation steam will be released into the free containment volume. The partial pressure of the released steam determines the over-pressurization of the containment during the accident. If the containment penetration valves have been rapidly closed upon indication of an accident the partial pressure of air remains close to 1 bar varying only slightly with the containment temperature. LOCA experiments confirm that the containment initially may be pressurized up to 2.5 - 4 bars still well below the design pressure of PWR containments.

Hydrogen combustion experiments have shown complete suppression of flammability at partial pressures of steam above 55-60 vol%, which approximately would correspond to the pressurisation caused by a blowdown of the primary coolant system. However, steam will condense on containment internal surfaces, initially at containment ambient temperature. After termination of the blowdown and even under conditions of moderate subsequent steam release from the primary system the partial pressure of steam finally may decrease below the

minimum threshold for complete suppression of flammability. A post accident containment inertisation could improve the situation.

Permanent inerting of the containment atmosphere offers the potential to completely and permanently suppress hydrogen combustion but suffers from the disadvantage of limiting access to the containment whenever access is required during normal plant operation. Permanent inerting has so far only been applied to BWR containments. Post-inerting (during a severe accident) is under consideration for PWR containments and does not suffer from this problem. A post-inertisation system should be able to replace condensing steam as the primary inerting agent and to preserve a sufficiently high inertisation degree. Carbon dioxide which is utilized in numerous industrial fire fighting systems could be used for this purpose. To ensure complete inflammability of hydrogen-air mixtures a maximum CO₂-concentration of 60 vol% is sufficient. Due to injection of the inertisation gas the pressure level inside the containment will be increased. The pressure should not exceed the level reached during a coolant blowdown, thus remaining well below the design value for the containment. If necessary, filtered venting may be finally activated to prevent slow overpressurization late in the course of a severe accident. Compared to the potential disadvantage of loosing the containment integrity by a hydrogen explosion a possible reduction of the available time span before initiating containment venting could be tolerated. At least, this should not be a credible argument against the selection of a post-inertisation procedure. Post-inertisation may also be applied in conjunction with the installation of catalytic recombiners. Sufficient oxygen will remain available although at low partial pressure allowing a continuous recombination of released hydrogen by catalytic recombiners.

Injection of liquid carbon dioxide released from a high-pressure storage tank is a proven technique [3,4]. Immediately upon discharge into the containment the temperature of the evaporating CO₂ will be in the range of 200 K (-73°C) providing a considerable driving head for buoyancy driven natural convection if injected at proper locations into the upper dome of a containment building. Main design goal for a post inerting system based on liquid carbon dioxide would be the preservation of a reliable inertisation status of the containment atmosphere. Pressure and temperature measurements available from the accident monitoring system could be utilized to initiate and control the necessary injection of carbon dioxide into a post accident atmosphere. These measurements may be interpreted in terms of an inertisation status of the atmosphere inside the containment. As long as the total containment pressure exceeds 2.5 bars complete inertisation is caused by steam partial pressure. To allow flexible activation of the CO₂-system the available CO₂ mass should be distributed within several tanks.

Severe accident analyses indicate that hydrogen formation may not be expected earlier than 1 hour after the initiating LOCA has occurred. Fire fighting experience with liquid carbon dioxide injection shows that total inertisation within a large volume compartment system would be easily achievable within a time window of 30 minutes.

Liquid carbondioxide, storage tanks, pipe systems and nozzles necessary for a post-inertisation system of a PWR-containment are technically available. The supply tanks should

preferably be located outside of the containment. The main CO₂ supply line penetrates the containment shell. Inside the containment the main pipe branches into smaller pipes ending in a number of nozzles distributed over the containment. A pressure of more than 5 bars upstream of the release nozzles must be warranted to prevent the system to be stuck by solid CO₂. Careful consideration must be given to the penetration necessary to transport liquid CO₂ through the containment shell. In particular, the penetration must also satisfy the needs for protection against thermal stresses. Industrial components are basically available to serve this purpose.

A threat to vital containment-internal equipment by possible cold shocks stemming from injected CO₂ has been raised as a counter-argument. However, pipe systems are normally protected by insulation. Other important aggregates like pumps etc. should not be target of CO₂-injection nozzles. Vital instrumentation and monitoring systems may in general more easily survive a period of reduced temperature rather than strongly elevated temperatures between 800 and 1000 K resulting from a large hydrogen fire.

3 Analytical Models

To simulate the flow of depressurising liquid and gaseous CO₂ the thermohydraulic code ATHLET [5] has been modified to simulate the transport of liquid CO₂. Using this modified code basic studies for the proper design of pipes and nozzles have been performed [7].

The gas distribution code RALOC [6] has been adjusted to allow the simulation of distribution processes associated with the injection of carbon dioxide at some predetermined locations within the containment. The formation and delayed sublimation of solid CO₂ has been modelled to take into account interim deposition of solid CO₂ on certain structures inside the containment. The CO₂-injection model has been validated using some experimental data obtained from flooding-tests for fire fighting systems [7]. A suitable parameter-set for the CO₂-model has been assessed reproducing the measured data taken from these experiments satisfactorily. Although external conditions like the nozzle-shape or the steam-concentration possibly influence the parameters. This combination has also been used to perform some analytical simulations for the HDR-facility and for the simulation of a large dry PWR-containment. Different scenarios have been analysed allowing basic insight into the aspects of post-inerting. Gas distribution analyses have been performed to predict the pressure and temperature evolution and to determine the location and the number of nozzles necessary to achieve and/or to preserve a sufficient degree of inertisation.

4 Post-Inertisation of a PWR-Containment

Based on available information about pressurized water reactors (PWR) an input data set has been developed modelling the containment of a typical German PWR (max. thermal Power 3900 MW, free Volume 70.000 m³) [fig.4.1]. The containment is simulated using 41 nodes [fig.4.2]. The spheric geometry was divided into symmetric arrangements of nodes each

representing one quarter-section. This input data set may still be general in some detail but it allows a comparison to results given in the German Risk Study [2].

4.1 Post-Inertisation of a PWR-1300 Containment After a Large Break LOCA

As initiating accident a large break LOCA was assumed to occur in node 2. The expected release rates for mass and energies have been derived by scaling up those during the HDR large break LOCA blowdown experiment T31.5. The annular space is connected to the inner compartments by several connections. The injection of liquid CO₂ was assumed to begin at 30 minutes after the initiating LOCA. Within 30 minutes a total amount of 183 tons CO₂ have been injected into the free volume of the containment of approx. 70.000 m³ at a rate of 100 kg/s. The injection nozzles were located in nodes 28 and 29, together representing one half sector of the dome region. The initiating LOCA causes a pressure peak of 3.1 bars [fig.4.3]. After 30 minutes the pressure has decreased to 1.35 bars due to surface condensation. Upon the injection of liquid CO₂ the pressure increases to 2.6 bars and subsequently by delayed evaporation to 2.8 bars. Fig.4.4 illustrates the temperature evolution at different locations of the containment. After initiating the injection of liquid CO₂ the atmospheric temperatures are dropping significantly and may locally reach minimum values of 22°C within the injection nodes. For this case the predicted volumetric concentrations of CO₂ [fig.4.5] indicate that the compartments in the upper region reach an inertisation degree above 60% within the period of 30 minutes, while some compartments of the middle and lower part reach this concentration with a delay of 30 to 60 minutes only.

This situation can be improved if the compartments of the middle and the lower part of the building are inerted separately. Fig.4.6 shows the volumetric concentration of CO₂ after splitting the injection. For this case 58% of the injection is applied to one half of the dome area (nodes 28 and 29), whereas 40% are injected into the lower steamgenerator-compartments (nodes 2,8,11 and 17). The pressurizer room (node 21) and the reactor pressure vessel room (node 22) have been inerted separately. The local CO₂-concentrations reach or exceed 60 vol% in all nodes after an injection period of 30 minutes. By this measure the inertisation time is reduced and demonstrating the flexibility of the inertisation strategy also offering the possibility of partial inertisation.

4.2 Post-Inertisation of a PWR-1300 Containment during an Anticipated LP* Scenario

Using the same containment data set the LP*-scenario [2] (loss of main-heat-sink due to loss of supply of electrical power coupled with intentional depressurisation of the primary system to prevent core-melt-through at high primary pressure) was simulated.

4.2.1 LP*-Case without Post-Inertisation

The release rates for water, steam and hydrogen shown in fig.4.7 have been taken from the German Risk Study [2]. Release of primary system coolant during the depressurization phase of the primary circuit was assumed to occur within the pressurizer room (node 22). During

the depressurisation phase a peak pressure of 2.2 bars is reached [fig.4.8, curve 1]. Afterwards pressure is increasing constantly reaching 3 bars after 6 hours. Here the simulation was stopped, assuming that the melt-through of the reactor pressure vessel could be successfully prevented. The otherwise possible interaction between core-melt and concrete has not been simulated because the core-concrete interaction process is not well understood for the release of core melt at elevated pressure levels of a failing vessel. This process may cause problems even more serious than hydrogen itself [8]. At begin of the hydrogen release the inertisation degree based on released steam differs between 3% in the sump area, 50-60% in the middle part and 45-50% in the dome area [fig. 4.9].

4.2.2 LP*-Case with Post-Inertisation

To improve this situation 2 hours after indication of the accident 91.8 tons of CO₂ - 50% of the amount that was chosen for the LP-case (sect. 4.1) - are injected within 15 minutes. The injection was subdivided as in section 4.1, resulting in a final pressure of 3.9 bars after 6 hours [fig.4.8, curve 2]. The volumetric concentrations of steam and CO₂ indicate that steam is replaced by CO₂ [fig.4.10 & fig.4.11]. The inertisation degree is increased significantly by this measure [fig.4.12]. Before the release of hydrogen the sump area reaches inertisation degrees of 50%, in the top region of more than 60% and for the middle part of the containment about 60% are predicted.

5 Supplementary Questions Related to CO₂-Inertisation

Using the modified ATHLET code [4] some information about the temperatures within the penetrations for CO₂-pipes through the containment shell have been provided. Fig.5.1 shows temperatures of liquid CO₂ inside a post-inerting pipe-system [7]. The storage temperature used was 20°C (curve1). During the injection period of 30 minutes it drops to approx. -10°C. Upstream of the simulated nozzles an initial temperature of 0°C is predicted dropping to -30°C after 30 minutes (curve 2). The temperature along the pipe system corresponds to the pressure inside the pipes so the temperature inside the penetrations must be in between these two curves shown.

The influence of solid CO₂ deposited on structures (e.g. coolant pipes was investigated using the modified RALOC code [6]. A heated steel plate of 1 m² having a thickness of 2cm was simulated as target of an injection nozzle. The steel plate is in contact with hot coolant fluid of 300°C on one side and the cold atmosphere nearby an injection nozzle on the other. The injection caused a drop of the surrounding atmosphere temperature to -78°C and the steel plate was continuously covered with solid CO₂. Fig.5.2 shows the surface temperature of this plate if the formation of a gas film is neglected (curve 1), if formation of a gas film is credited (curve 2) and if the plate would be covered by a 10 cm layer of insulation material (curve 3).

If the gas film formation between solid CO₂ and the steel plate is neglected (high heat transfer coefficient assumed) a significant drop of the surface temperature from 295°C to 230°C

within 5 minutes is predicted. Assuming that a gas film formation between the steel surface and deposited CO₂ takes place the temperature decrease is limited to not more than approx. 5°C. A steel surface protected by an insulation keeps its temperature nearly constant as could be expected.

6 Conclusions

The optimal location for injection of liquid CO₂ would be in the upper dome area preferably designed as an asymmetric injection. Supplementary injection nozzles may be installed in some special compartments of the containment. Such a procedure results in a rather uniform concentration distribution of CO₂ and steam exceeding 60 vol% during the chosen injection period of 30 minutes all over the containment. Under these conditions no more flammable hydrogen-air mixtures are to be expected. The injection of CO₂ resulted in only moderate change of room temperatures to be expected. In case of scenarios with long lasting blowdown phases inertisation should be initiated by injecting only a part of the stored CO₂. To allow this flexibility the CO₂ storage should be divided into several tanks. If necessary additional CO₂ tanks may be activated later. The necessary information for an operator to continue with the injection could be provided by the accident monitoring system which will support any accident management measure.

A possible threat to vital containment-internal equipment by cold shocks stemming from injected cold CO₂ has shown not to be a real problem. Pipe systems are normally protected by insulation. Other important aggregates like pumps etc. should not be target of injection nozzles as well as vital instrumentation and monitoring systems which may more easily survive a period of lower temperatures than a period of very high temperatures caused by a hydrogen fire.

The installation of a sufficient number of catalytic recombiners remains possible to finally allow the recombination of the entire released hydrogen. Sufficient oxygen will be available although at lower partial pressure still allowing a continuous recombination of released hydrogen by catalytic recombiners. Tests performed with recombiner materials have shown that the presence of CO₂ has no negative impact on the efficiency of the recombiners. The presently advised design goal to recombine the released hydrogen within 24 hours would not be impeded by the activation of the CO₂-injection. Depending on the thermodynamic and the radiological status of the containment atmosphere the activation of the filtered venting system remains also possible.

The results of the presented study are encouraging, nevertheless some supporting experiments would be of interest to continue confirmatory code validation. Some experimental facilities would still be available to become involved in such tasks.

Acknowledgement

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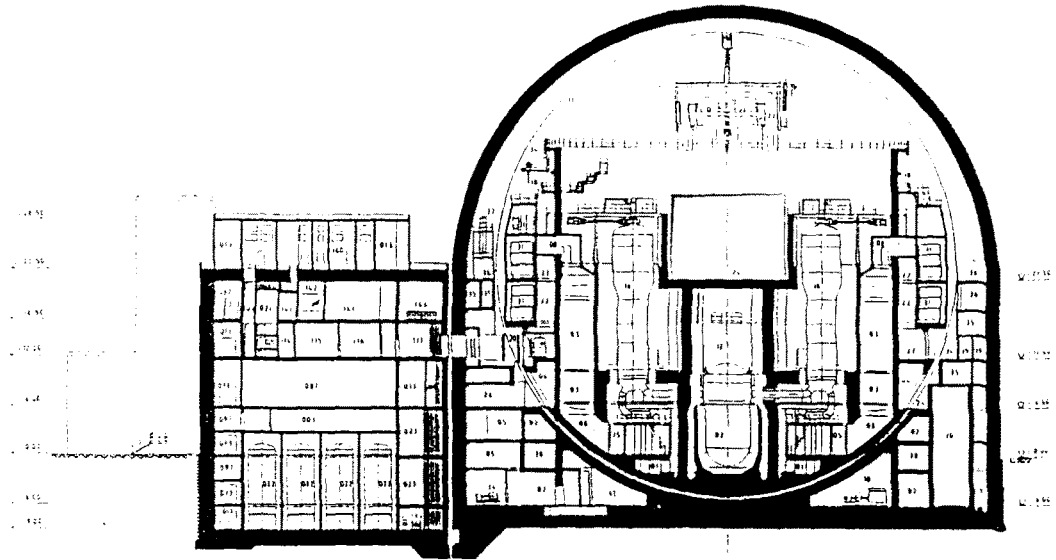


Fig. 4.1 PWR Containment

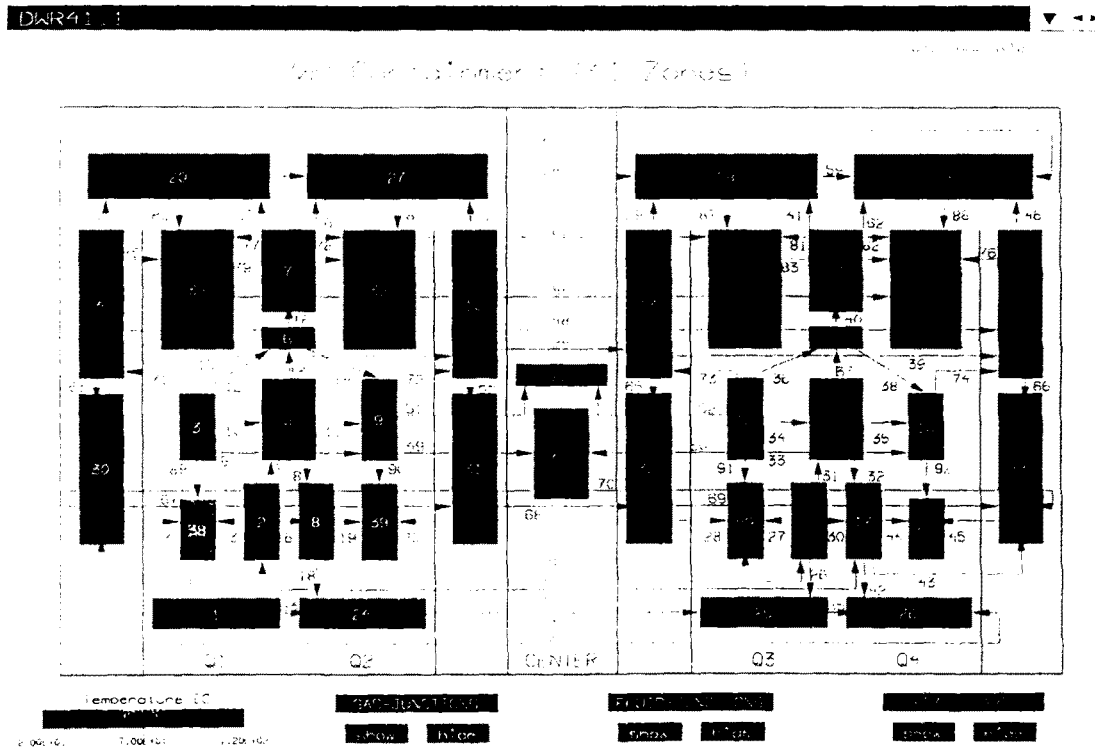
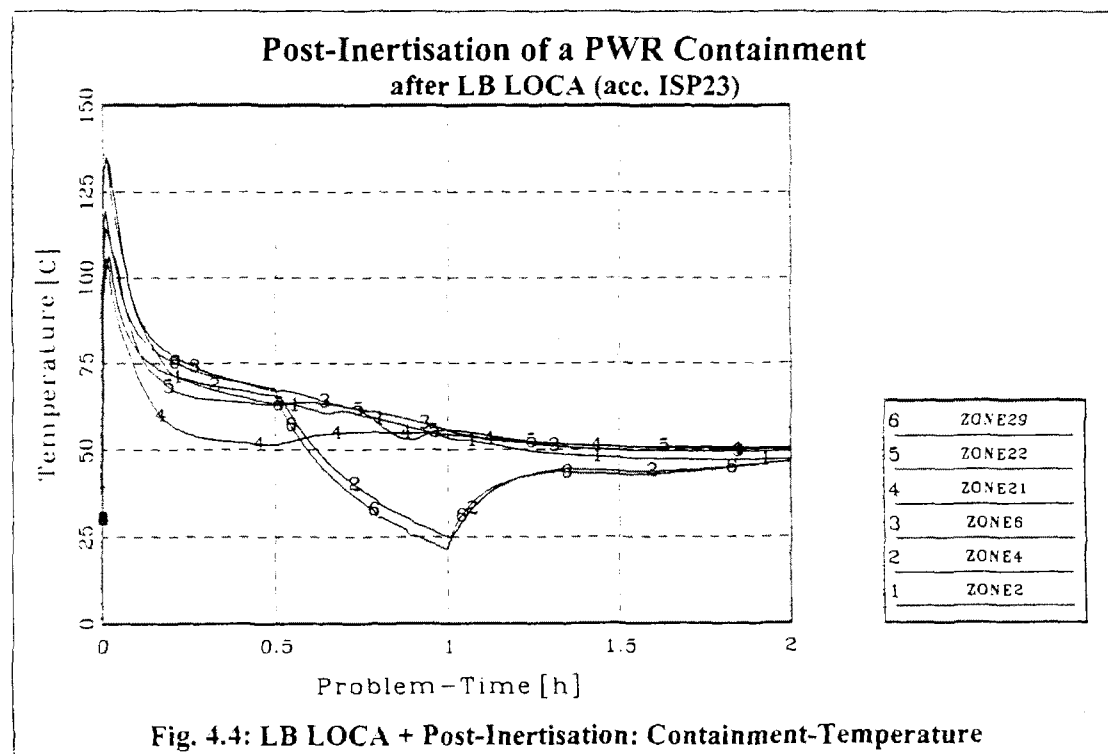
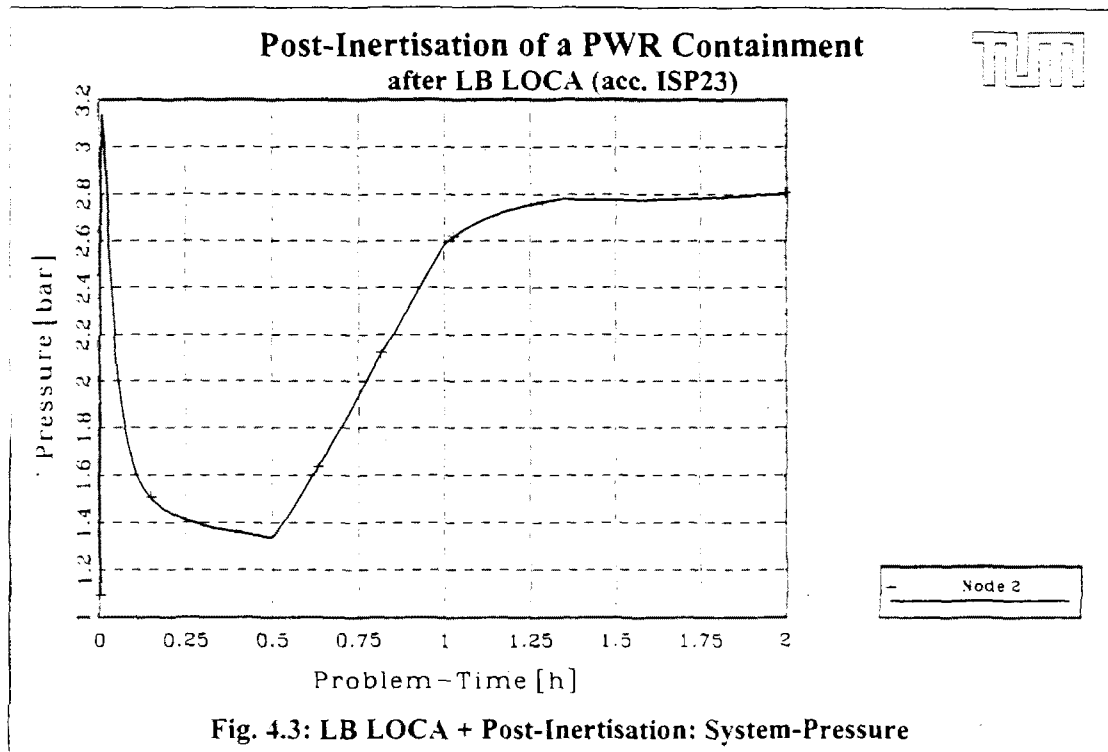


Fig. 4.2 Nodalisation of PWR Containment



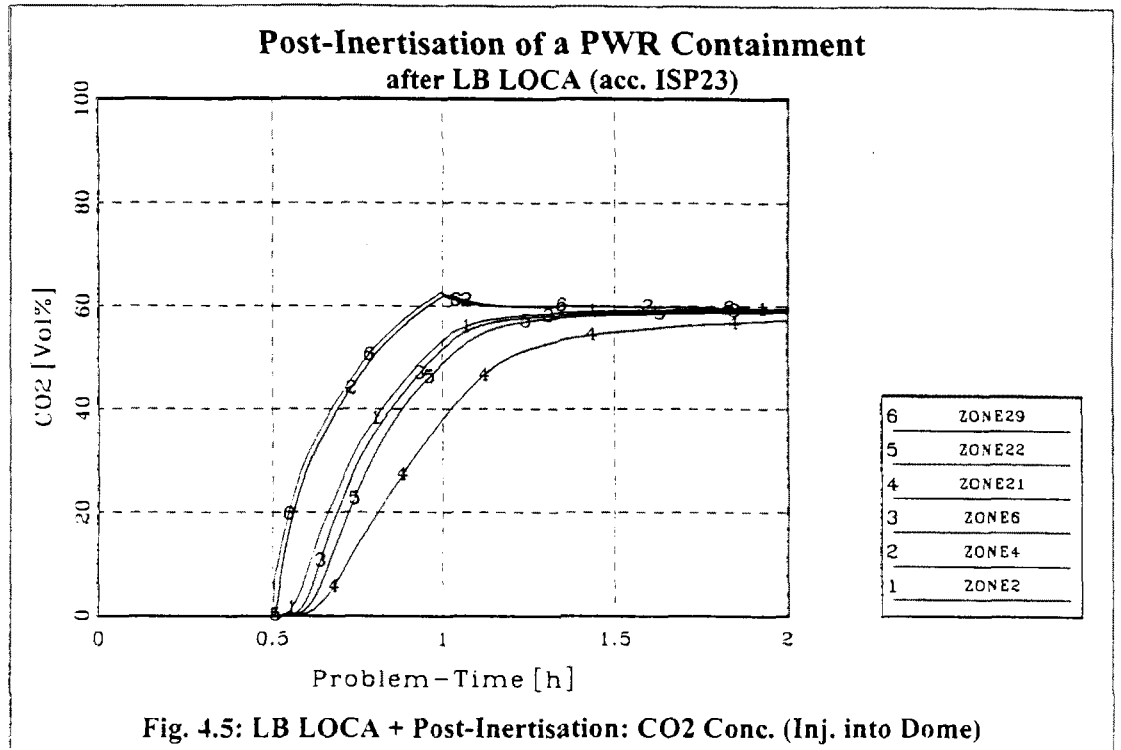


Fig 4.5: pl 24/03/96 10:09:31

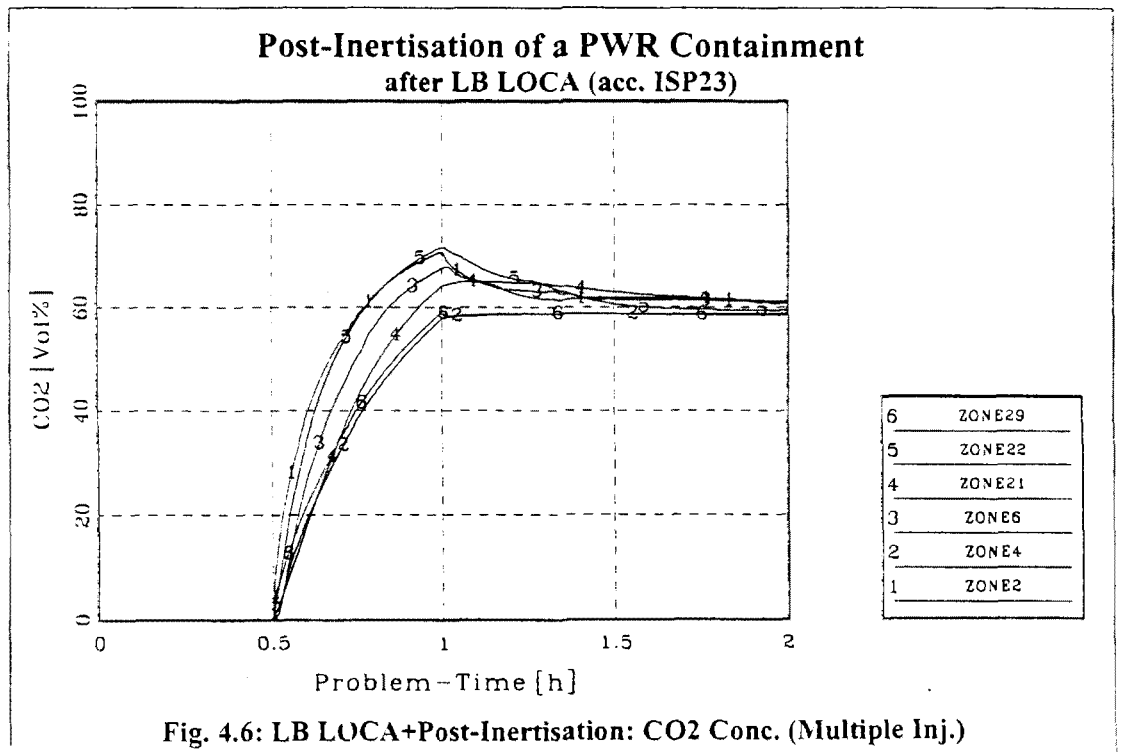


Fig 4.6: pl 24/03/96 10:09:53

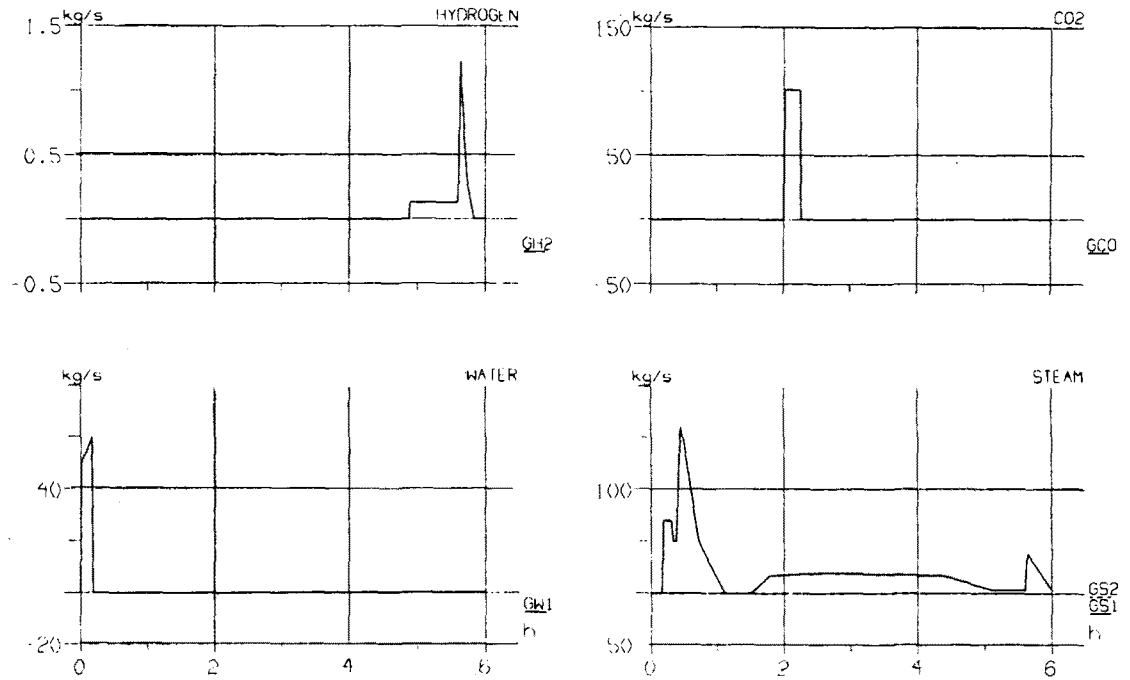


Fig. 4.7: LP*-Case: Injection Rates

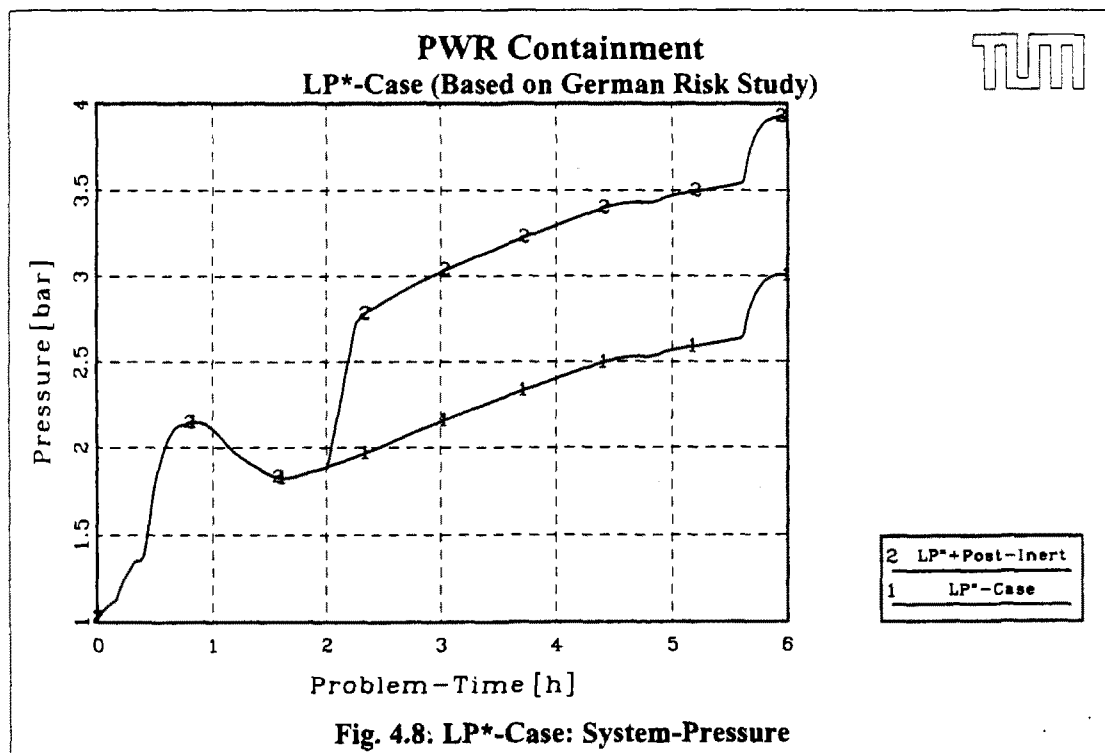


Fig. 4.8: LP*-Case: System-Pressure

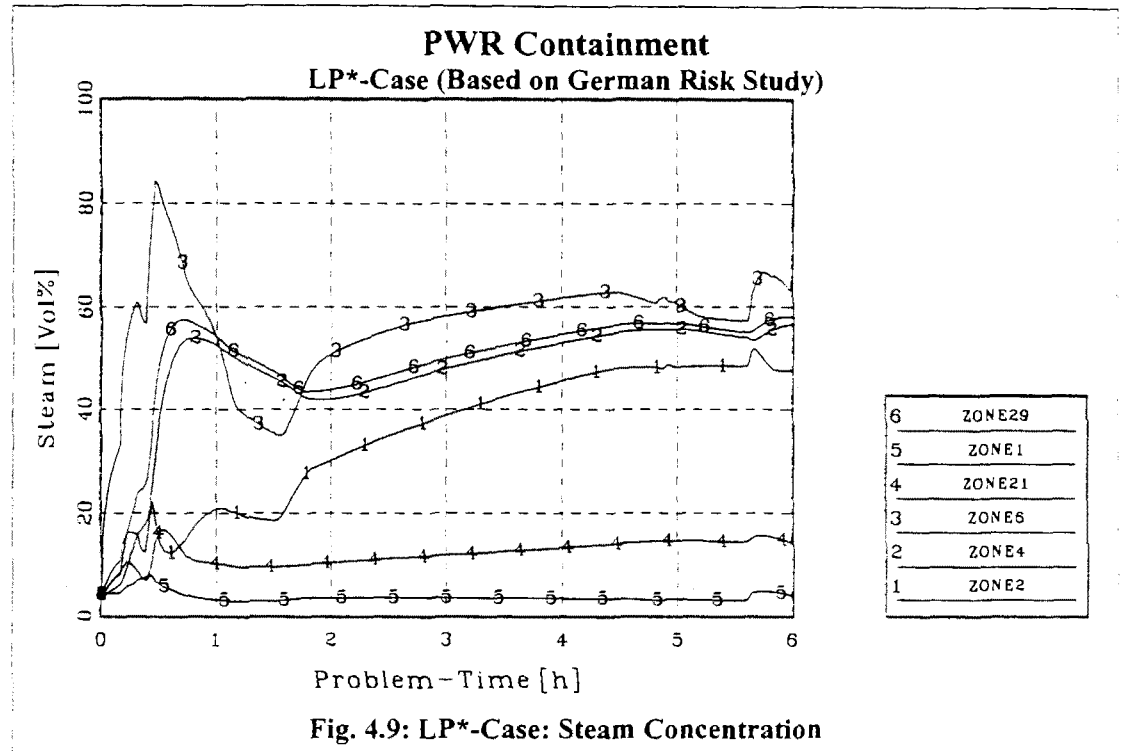


Fig. 4.9: pl. 24/03/96 10:10:26

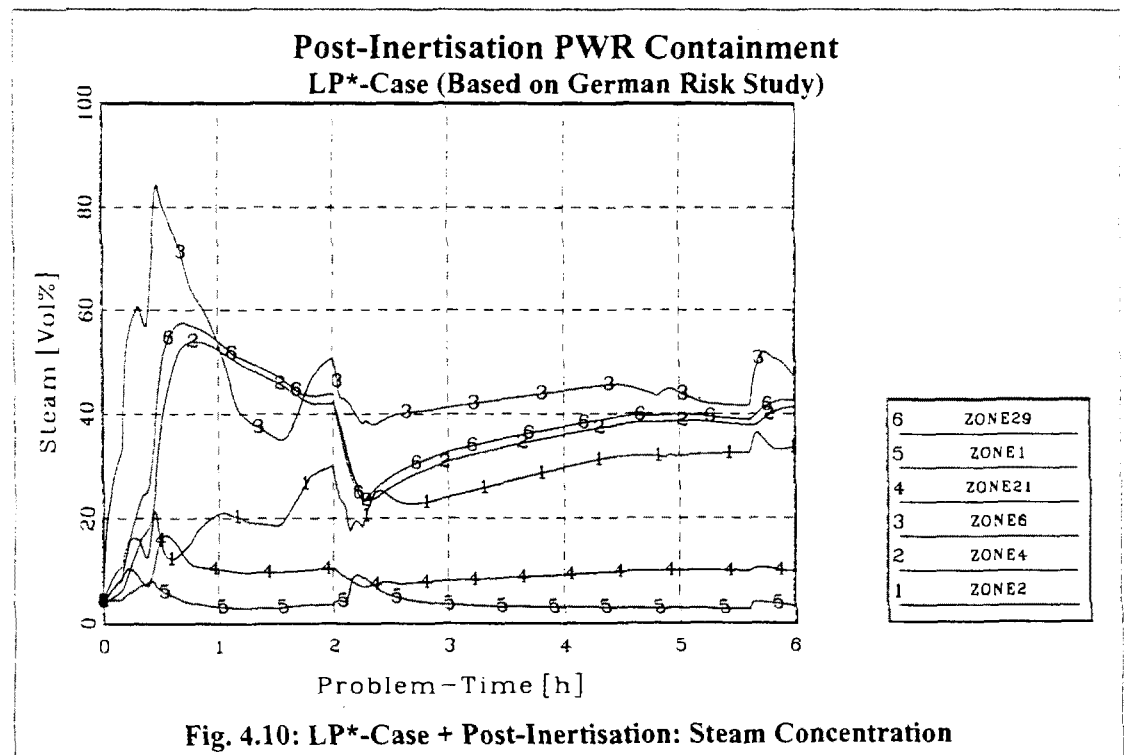
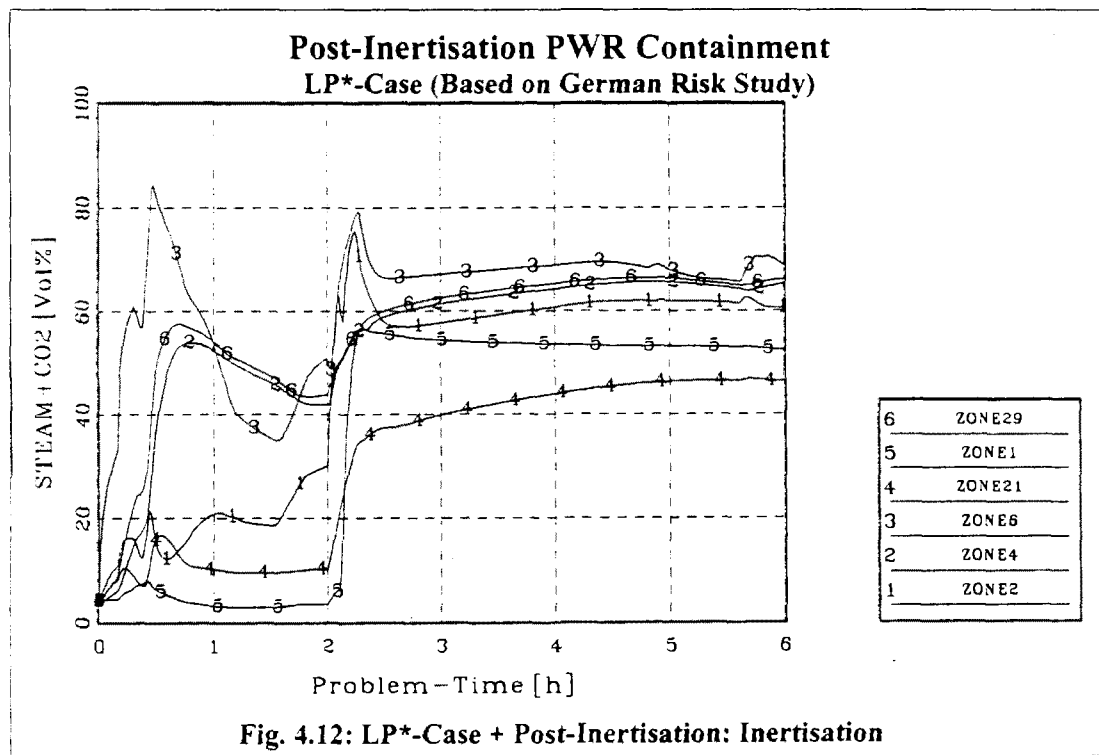
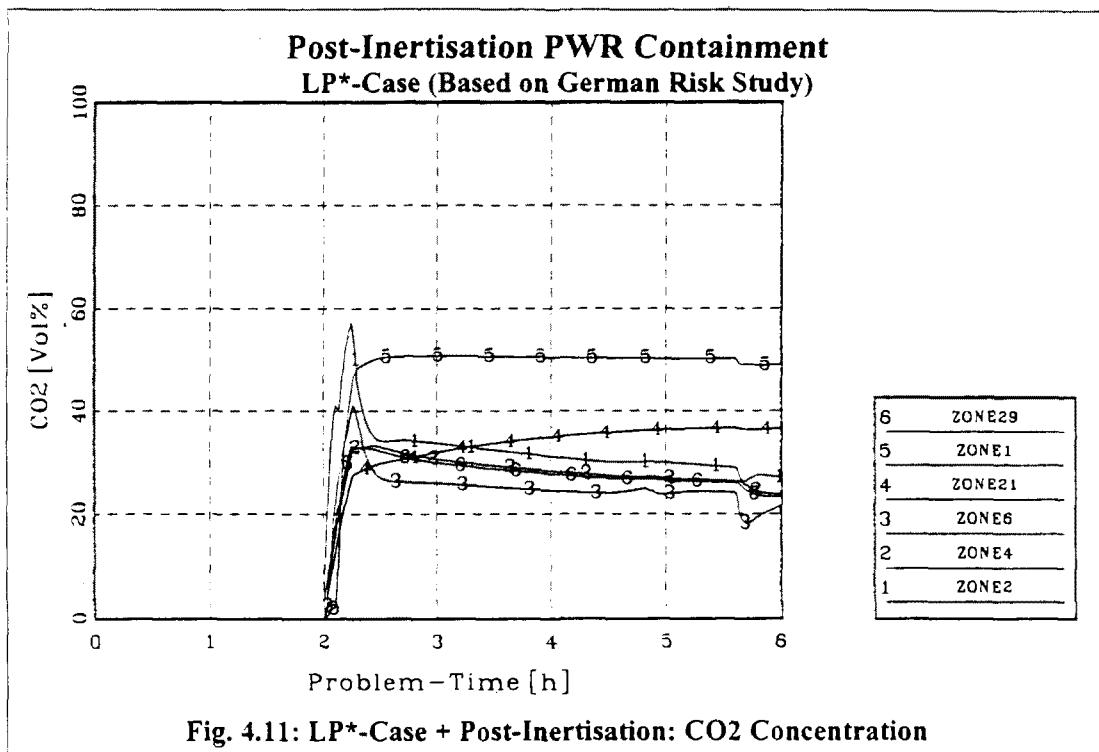


Fig. 4.10: pl. 24/03/96 10:11:27



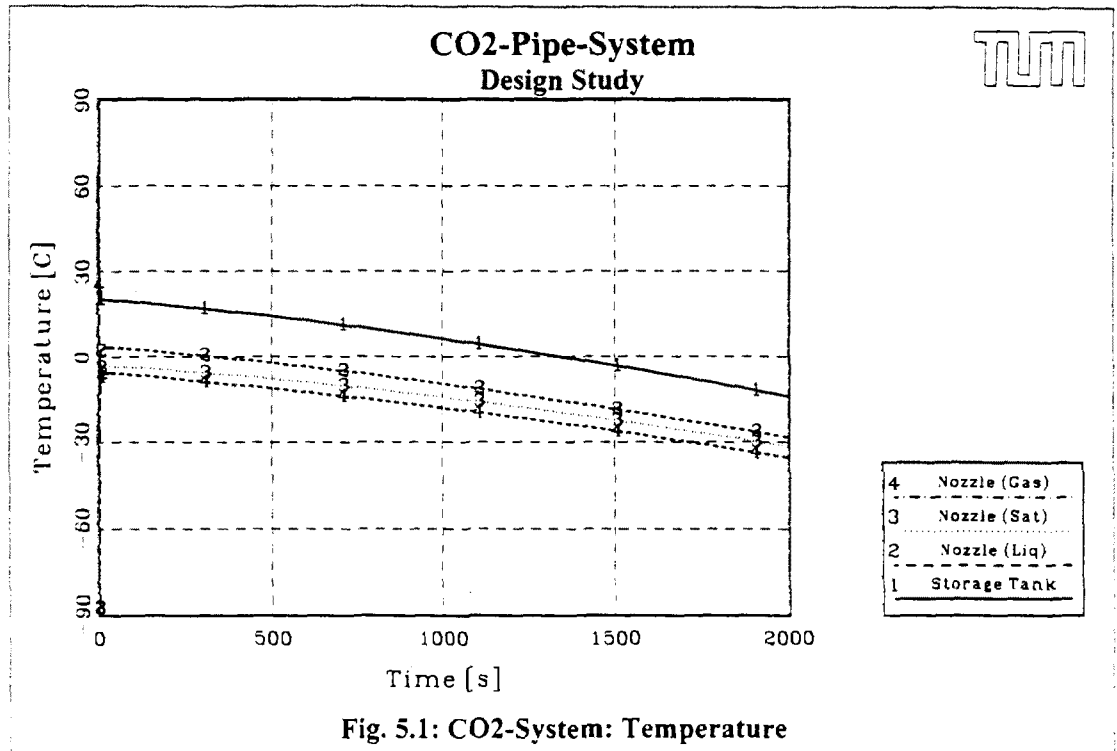


Fig 5.1: pl 24/03/96 10:18:04

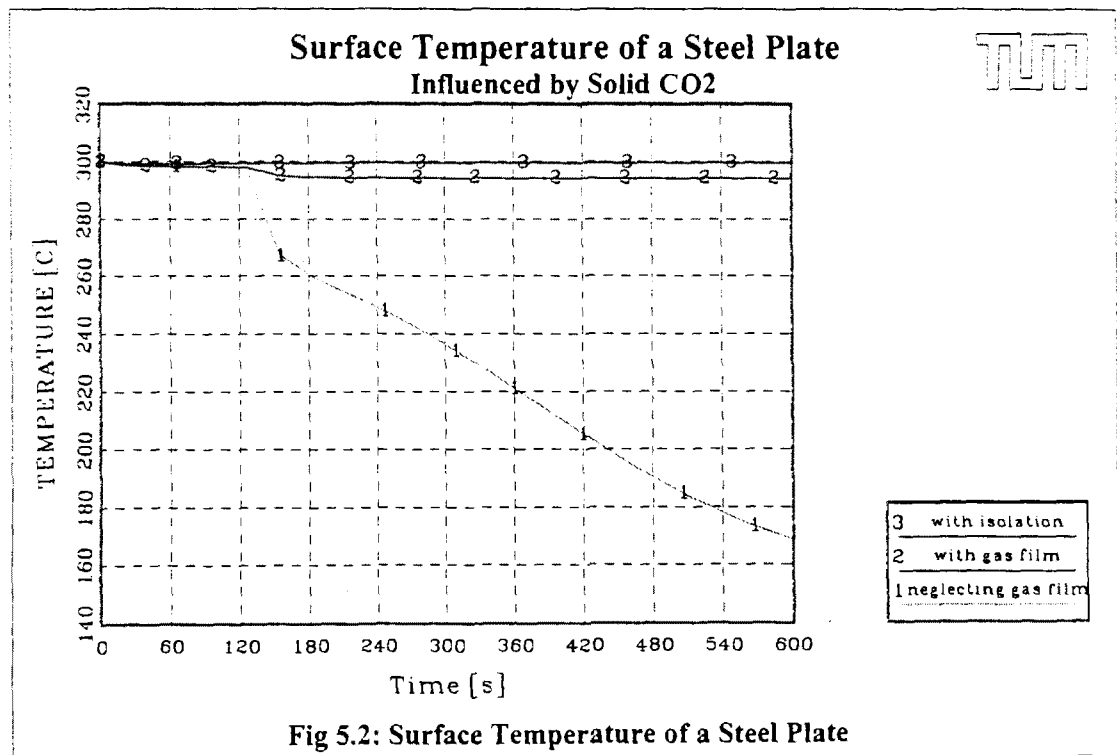


Fig 5.2: pl 24/03/96 16:54:04

4.3 SEMI-PASSIVE PAD (POST-ACCIDENT DILUTION) SYSTEM COMBINED WITH RECOMBINERS OR IGNITERS FOR e.g., MULTIPLE-UNIT VVER

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Abstract

In order to reduce the risk associated with hypothetical severe nuclear accidents, it has been necessary to develop means for hydrogen control which are capable of handling high H_2 production rates of several m^3/s . These systems and components should be of simple and, as far as possible, passive design. Moreover, their efficiency must not be affected by the H_2 production scenario. To meet these requirements, Siemens has supplemented its dual process for hydrogen reduction using recombiners and igniters by developing a new process: the semi-passive Post-Accident Dilution (PAD) System.

The objective of the work presented in this paper was to design an alternative approach for hydrogen control comprising post-accident CO_2 dilution combined with recombiners or igniters for postulated severe accidents that result in significant hydrogen releases.

The combined technique was to keep the dynamic loads arising inside the containment to within a range of only a few millibars, even in the event of transient hydrogen release and distribution processes accompanied by combustion, and was thus to prevent any hazard for safety-related equipment.

The first part of the paper describes the relevant system requirements determined for severe accident H_2 release scenarios, such as:

- H_2 release rates of 0.15 to 3 kg/s
- available CO_2 injection time of 20 to 30 minutes
- additional CO_2 concentration of approximately 30% by vol.
- injection rates of 30 kg/s
- measures for protection of personnel, and
- H_2 recombiner depletion rates, etc.

After this, a comparison of the features of a liquid CO_2 injection system with those of a gaseous CO_2 injection system is presented which was used to select the optimum inert gas phase. The paper then describes the new technology of semi-passive post-accident dilution (PAD) which has been developed by Siemens to avoid the problems encountered with the liquid CO_2 injection systems known to date, such as thermal shock, CO_2

flocculation, blockage at pressure drops to below 5.18 bar, distribution and other difficulties specifically related to liquid gas techniques.

The PAD System has been designed to meet specific process requirements such as, in particular, prevention of fast deflagrations and DDT phenomena, both locally and globally, in the various containment geometries. Using the specified process data as a basis, the PAD System was then combined with a small number of recombiners or igniters.

The efficiency of the PAD System combined with, for example, 20 recombiners or igniters was checked by means of multi-compartment analyses and compared to that which could be achieved by a system combined with a larger number of recombiners, namely 42.

Under the ambient conditions created by the PAD System, hydrogen can be reliably reduced - even if the release rates are postulated to be high and/or hydrogen accumulation takes place inside areas which are temporary steam inerted - by using only a few recombiners (or igniters) and without any risk of fast deflagrations or DDT phenomena inside the containment.

Prototype test results for the semi-passive PAD System, including the achieved CO₂ distribution, are then presented.

The results of the tests have shown that the new semi-passive system is capable of feeding, for example, 50 Mg of CO₂ at a suitable temperature of > 5°C into the containment within the available time of less than 20 to 30 minutes.

Integration of this semi-passive PAD System into existing plants is much easier since the gaseous injection technique allows existing pipes to be used and because it does not require a complex distribution system inside the containment or confinement. An extremely cost-effective situation compared to other approaches for H₂ control is given for multiple-unit VVER plants since, at such plants, the CO₂ can be supplied from a single central PAD System, thus considerably reducing the effort and cost involved in employing such a system.

1 Introduction

During severe accident scenarios in nuclear power plants equipped with light water reactors, large quantities of hydrogen may be released into the containment atmosphere within a short period of time. Especially in large dry containment's- without steam condensing devices like ice condenser or suppression pool - hydrogen accumulation could be postulated during temporary steam inerted conditions in certain containment regions which leads finally to new situations in terms of distribution and possible flame regims.

Late ignition of these mixtures can lead to uncontrolled turbulence-accelerated combustion processes which are characterized by high combustion rates and corresponding pressure loads. This in turn could jeopardize the integrity of the containment and thus of the final barrier for preventing a massive release of radioactive fission products to the plant environs.

Particular significance is therefore attached to measures for hydrogen reduction inside the containment.

In order to further reduce the risk of an early or late loss of containment integrity at pressurized water reactor (PWR) plants as a result of hydrogen combustion processes in the event of severe accidents, the German Reactor Safety Commission (RSK) recommends that supplementary methods and systems be utilized.

Similar requirements for H₂ control under severe accident conditions are also being discussed and imposed in connection with VVER plants.

1.1 Process Development

As a measure aimed at preventing uncontrolled turbulence-accelerated combustion processes, Siemens developed its "dual concept" comprising autonomously operating igniters and passive autocatalytic recombiners (PARs).

Siemens' PARs are presently being manufactured for and have already been installed at a number of European nuclear power plants.

In addition, Siemens has now developed a further technique for the general prevention of fast flame propagations which is also effective in the event of the release of large quantities of hydrogen - semi-passive post-accident dilution (PAD) using CO₂ - and has already tested a prototype of this new PAD System.

This paper will present in detail the basic system design requirements and criteria, the options available in terms of process engineering, the efficiency of the process and the prototype test of its semi-passive PAD System.

The efficiency of the process in combination with recombiners or igniters will be examined as well.

2 Hydrogen Control Requirements

2.1 Basic Requirements

The basic objective behind hydrogen control is to suppress the formation of highly reactive hydrogen concentrations and the occurrence of fast flame propagations in the event of severe accidents with massive hydrogen releases into the containment and thus to keep the loads imposed by H₂/O₂ reactions on the containment and its safety-related internals within allowable limits of a few 100 mbar.

This means, for example, that measures for hydrogen control must be designed such that the problems associated with hydrogen releases and redistribution can be effectively solved in all postulated situations, such as, for example, the following:

- Early/late release of small/large hydrogen quantities
- Hydrogen release coupled with large/small quantity of steam
- H₂/H₂O jet releases
- Conditions under which hydrogen concentrations can arise in specific zones of the containment during steam inerted conditions(e.g. inside the missile shield).
- H₂ redistribution during combustion

In addition, the concept should feature a simple design, should incorporate passive equipment to as large an extent as possible, should be cost-effective, and should be easy to integrate into various types of plants.

2.2 Considered Methods for Hydrogen Control

On the basis of the aforementioned objectives, different processes for post-accident CO₂ dilution have been developed and evaluated with regard to their efficiency.

Their efficiency was checked by means of analyses performed using the WAVCO multi-compartment code, these analyses being based on the following two approaches:

- Recombiners used alone
- Post-accident CO₂ dilution plus recombiners.

2.3 Process Requirements

The main process requirements which must be met by a post-accident CO₂ dilution system are outlined below:

- Extensive use of passive process equipment
- Non-scenario-dependent distribution of CO₂ inside the containment/confinement prior to the release of significant quantities of hydrogen
- No activation of CO₂ injection in the event of controlled loss-of-coolant accidents (LOCAs)
- Prevention of thermal shocks upon CO₂ injection
- Accident-induced damage to the CO₂ injection equipment must not compromise the basic effectiveness of the system
- Protection of personnel through
 - Prevention of inadvertent system activation
 - Alarm annunciation and assurance of ample time for evacuation
- Independence from external power sources (station blackout).

2.4 H₂ Release Scenarios

In order to be able to demonstrate that means for hydrogen control are effective regardless of the specific accident sequence in question, it is necessary to determine enveloping H₂ release rates.

Since this new system design concept is particularly aimed at solving local H₂ problems, it is necessary to specifically determine enveloping maximum H₂ release rates.

The following enveloping scenarios were investigated in detail:

- a) H₂ generation inside the reactor pressure vessel (RPV) when water and steam come into contact with high-temperature structures in the core region in cases in which core degradation was only stopped by a late restoration of core cooling through reflooding (= in-vessel scenario)
- b) H₂ release inside the RPV as well as due to concrete-melt interaction in cases in which it was not possible to restore core cooling and RPV melt-through occurred (= ex-vessel scenario).

The in-vessel analyses (Ref. 3) were based on the following accidents:

- Pipe break resulting in loss of coolant with failure to switch over to sump extraction for coolant supply
- Loss of steam generator feedwater supply together with failure of the active emergency core cooling systems following successful primary-system depressurization.

As regards the quantity of hydrogen that can be released, it should be conservatively assumed in order to satisfy e.g. RSK's requirements or others - and to apply values that are higher than the results of the in-vessel analyses - that in case a) the entire Zircaloy inventory in the core oxidizes (approx. 1350 kg H₂) and that in case b) there is an additional release of hydrogen due to concrete-melt interaction (approx. 650 kg H₂).

The analyses - which were performed using MAAP3.0B as well as STCP - yielded the following results:

- The generated quantity of hydrogen is between 370 and 590 kg; this corresponds to oxidation of 25 to 40% which increases when slumping or reflooding is included in the considerations
- Depending on the scenario being investigated, the maximum hydrogen release rates range between approximately 0.15 and 3 kg/s
- For the scenario selected as being of greatest significance - namely 50-cm² break cross section and earliest possible start of hydrogen release, the release of hydrogen can be expected to begin about 20 to 30 minutes after the water level inside the RPV has dropped below "very low".

Figure 6 shows the enveloping H₂ release rates and quantities determined for the two conservative reference scenarios which were to be used as a basis for designing the hydrogen control system.

The results of the above analyses were employed to define the following enveloping requirements to be met by the PAD system.

2.5 Resultant Additional Requirements for Post-Accident CO₂ Dilution

- Consideration of enveloping H₂ release rates of, for example, 0.15 to 3 kg/s and H₂ quantities equivalent to Zircaloy oxidation of up to 100%
- Achievement of suitable CO₂ concentrations in the relevant zones of the containment atmosphere for:
 - Prevention of DDTs
 - Prevention of fast flame propagation, even in the presence of turbulence generators
- Non-scenario-dependent homogeneous CO₂ distribution in the relevant zones of the containment prior to the release of significant quantities of hydrogen
- Limitation of the maximum global H₂ concentration inside the containment (by means of recombiners or igniters) so that the containment test pressure will not be exceeded in the postulated event of adiabatic, isochoric complete combustion (AICC).

3 Suppression of Fast Deflagration and DDT Prevention using Inert Gases

3.1 Basic Principles

Figure 3 shows the basic combustion regimes obtained with steam dilution. These are anticipated to be largely similar to those obtained with CO₂. The benefits expected from CO₂ injection are that it will alter the composition of the gas mixtures in the relevant zones of the containment in such a manner that only slow deflagration can take place or that even inert conditions will be created.

CO₂ injection can, as a rule, produce slow combustion processes with flame propagations of only a few 10 m/s, and can also make it much easier to verify that combustion loads can be accommodated, even in the case of containments with internals designed for lower differential pressure loading. Moreover, regardless of the local H₂ release scenario, fast deflagrations or DDTs can be ruled out.

3.2 Experimental Verification

In connection with H₂ combustion tests, there are several studies (Refs. 2, 3 and 4) which have investigated the effect of inert gases on combustion processes ranging from fast flame propagation up to DDT. More recent experimental findings are available from the Prints Maurits Laboratory in the Netherlands (Ref. 3) where tests were performed to examine the effect of inert gases (CO₂ and N₂) on H₂/air combustion in test geometries equipped with obstacles (turbulence generators).

These tests investigated the influence of the inert gases on the combustion pressure and velocity as a function of the gas composition and the degree of restriction of the geometry (turbulence).

Figure 4 shows the effect of an atmosphere diluted with CO₂ on the flame speed and the maximum combustion pressure for different CO₂ concentrations and for degrees of restriction of, for example, 32%.

Figure 4 shows that with a stoichiometric mixture and a degree of restriction of 32%, the addition of approximately 15 to 20% CO₂ is sufficient to ensure that combustion processes consist of only slow deflagrations. The analyses also show, however, that with higher degrees of restriction (e.g. 50%), 20% CO₂ is not sufficient to shift a stoichiometric mixture into the area of slow deflagrations - a higher percentage of dilution with CO₂ is necessary for this.

A comparison of the effectivenesses of CO₂ and N₂ shows that, on average, approximately 8% by vol. more N₂ is required to obtain the same effect as with CO₂.

The experiments described in References 2, 3 and 4 were performed on a laboratory scale. In order to determine the scalability of these results, the bodies that conducted the experiments recommend themselves that large-scale tests (e.g. in the Battelle model containment) be carried out.

4 Systems for Post-Accident Dilution of the Containment Atmosphere

In the following, different CO₂ injection systems will be investigated and analyzed with respect to requisite process characteristics. In this connection, a fundamental distinction must be made between those systems which inject gaseous CO₂ into the containment and those which inject liquid CO₂.

4.1 Liquid CO₂ Injection

4.1.1 System-Specific Design Criteria

In the case of liquid CO₂ injection, it is essential that a reduction in system pressure to below 5.18 bar be prevented.

Reason:

CO₂ instantly solidifies at pressures below 5.18 bar, something which leads to system blockage.

Problematical operating conditions:

Startup:

- Pressure must be kept continuously above 20 bar right up to the containment isolation valves
- Quick-opening containment isolation valves are needed
- Pressure must be kept above 5.18 bar even in those sections of the system located inside the containment during liquid gas injection (signal race condition).

Operation:

- The system pressure must still be maintained above 5.18 bar even if part of the system's equipment should be destroyed in the area affected by an accident.

4.2 Configuration of Liquid Injection System

This system comprises a storage tank for liquid, low-temperature CO₂, connecting piping as well as new low-temperature injection lines complete with containment isolation valves and a distribution system installed inside the containment. Upon injection of the CO₂, expansion causes the triple point to be reached as shown in Figure 4; this leads to a drop in temperature to -79°C accompanied by partial spontaneous evaporation and simultaneous flocculation of the CO₂. Due to the highly concentrated mode of injection (compared to conventional fire extinguishing systems) into, for example, only two compartments or areas, approximately 50% of the injected CO₂ can be expected to undergo flocculation (see also the right-hand side of Fig. 5 taken from Ref. 5). This CO₂ will later start to evaporate after deposition on cold surfaces (structural heat sinks).

4.3 Evaluation of Liquid CO₂ Injection

Due to the volume of the containment and its subdivision into various zones, injection of liquid CO₂ into the containment atmosphere can only be performed if, among other things, a correspondingly extensive distribution system is provided inside and outside the missile shield.

In the case of this form of transport (the same as that typically used in conventional CO₂ fire extinguishing systems), the high safety requirements to be met with regard to the primary system, the emergency core cooling and residual heat removal systems and the containment/confinement itself, etc. make it extremely difficult to analyze the efficiency of this method of injection under normal and abnormal system operating conditions, as earlier reported in Reference 6. Problems arise in connection with the following, for example:

- Possibility of low-temperature CO₂ jets (-79°C; see left-hand side of Fig. 5) impacting safety-related equipment with high operating temperatures (including surfaces exposed after their insulation has become detached as a result of accident-related effects)
- New low-temperature loading conditions for internals
Problem areas:
 - Instrument leads
 - Primary-system components (possibly without insulation)
 - Valves
 - Internal structures and platforms
 - Cables, etc.
- Material analysis in the nil-ductility transition range or analysis of valve operability under conditions outside the normal range for functional testing.

Therefore, liquid injection is not recommendable on account of these severe safety-related disadvantages as well as the large effort that is required (e.g. for installing additional low-temperature piping and penetrations, etc.).

In order to avoid these disadvantages, new gaseous injection systems have been developed.

5 Gaseous CO₂ Injection

Gaseous CO₂ injection systems have been the subject of a number of studies. However, such systems were found to be too expensive and complicated and were consequently discarded for reasons associated with certain aspects of the technology available at that time, such as:

- Gas storage in compressed gas cylinders, or
- CO₂ evaporation using burner systems with thermal outputs of over 10 MW.

5.1 Semi-Passive PAD System

After designing and comparing various processes for producing high gaseous CO₂ flow rates, Siemens selected the semi-passive PAD System as being the most cost-effective variant. This system was then developed in greater detail and subjected to prototype testing.

5.1.1 System Design Criteria

The design had to take the following criteria into account:

- Concentration of injected CO₂ in the containment = approx. 30% by vol. (50 Mg of CO₂ to be injected)
- Duration of injection ≤ 20 min
- Injection rate > 30 kg/s
- Time required for system activation ≤ 10 min
- Use of existing piping such as venting lines etc. must be possible (avoidance of low-temperature loading)
- It must be possible to interrupt injection
- Personnel safety must be ensured in the event of system activation.

5.1.2 System Configuration

The system comprises the following:

- A module for storage of liquid, cold CO₂ with integral permanent energy store and equipment for direct evaporation
- Already existing gas injection lines and associated containment isolation valves (e.g. venting lines with nominal diameters of 100 to 200 mm, or similar)
- One or two high-speed mixing and distribution jets.

A schematic representation is given in Figure 10 for a VVER plant.

By installing an additional module the system can also be employed for conventional fire fighting in various areas of the plant.

5.1.3 System Operating Modes

The system is either in the standby mode or is operated in the CO₂ injection mode.

5.1.4 Standby Mode

While operation of the CO₂ injection system is not required, it is kept in a permanent standby mode at full operating pressure and temperature.

Refrigeration equipment is used to maintain a constant CO₂ pressure in the storage tank.

The energy store is continuously heated to keep it permanently charged. After initial heatup, only normal heat losses have to be made up. Furthermore, the store is sized and insulated such that, in the event of power being lost for up to e.g. 24 hours, sufficient energy will still be available to allow all of the stored liquid CO₂ to be evaporated.

The power required for the standby mode is < 5 kW.

Pressure and temperature control is fully automatic.

5.1.5 CO₂ Injection Mode

The system is activated from the central control room. The module provided for this must first be enabled using a key-operated switch. Activation is performed in accordance with a procedure set forth in the emergency operating manual.

CO₂ injection is activated in the event of, for example, the following:

- RPV water level < "very low"
- or
- Fuel assembly exit temperatures > "very high".

It is possible to interrupt and restart CO₂ injection from the system control panel.

In the event of CO₂ injection, visual and audible alarms are activated inside the containment to alert personnel.

Since injection takes place primarily inside the missile shield, more than 5 minutes will elapse before a CO₂ concentration of 5% by vol. is reached in the vicinity of the reactor service floor. Since this concentration can still be tolerated by the human body, sufficient time is available - even in the event of inadvertent activation of the system - to allow operating personnel to leave the containment.

When operation of the system is required, the energy storage medium is discharged into the CO₂ storage tank. This causes instantaneous evaporation of the CO₂ at an elevated pressure which can be selected within specified limits, as well as simultaneous heating of the gaseous CO₂ to a temperature above, e.g., 30°C. Heat transfer is effected through direct contact of the fluids. This enables high heat-transfer coefficients to be achieved and thus a direct transfer of energy with a low terminal temperature difference and with efficient utilization of the stored energy. Operation of the prototype system is shown in Figure 12.

In a full-scale system, the required CO₂ injection rate would be kept constant within a preselected range of, for example, 30 kg/s and above by adjusting the rate of stored energy discharge.

5.2 CO₂ Distribution Inside Containment

As analyzed in Reference 7, conventional methods for CO₂ injection - lacking the momentum mixing effect provided by jet-type injection - do not result in sufficient distribution of the CO₂ so that severe inhomogeneities can arise. For this reason, a high-speed jet injection technique was developed and tested in order to prevent such effects from occurring.

5.2.1 High-Speed Jet Injection

Inside the containment, injection and mixing of the CO₂ with the containment atmosphere can be effected by means of, for example, supersonic nozzle injectors which convert pressure to velocity.

The high motive pressure of > 10 bar that can be made available and the design of the injector allow a quantity of containment atmosphere equivalent to up to ten times the volume of injected CO₂ to be drawn in. The mixture is discharged by the injectors at high velocity into the containment atmosphere. The momentum imparted by the injected gas jet provides for thorough mixing of the atmosphere. The range of the jet under quiescent conditions determined during the tests is > 30 m at a motive gas flow rate of 1.5 kg/s (see also Fig. 11).

The mixing effect inside the injector and subsequent free-jet mixing in the injection zones both inside and outside the missile shield were calculated - on basis of the prototype test results and using the average values of three different empirical free jet equations for determination of the atmosphere mixing, e.g. /Ref 1; 8/- and taken into consideration in the WAVCO analyses. The results for 18 kg/s CO₂ injection are shown as an example in Fig.13. The injector creates a volume flow of more than 600 m³/s in a distance of 55 m. It was found that, during the period of injection, the atmospheres inside and outside the missile shield are remixed a total of more than 20 times. This would correspond to a total volume remixing factor of more than 60 per hour. This value represents - also in relation to the change of air factors used in the air ventilation technology for high flushed areas /Ref 1/ of (15 to 20 h⁻¹) - a comparable very high value, which always describes an extreme homogenous distribution situation.

As also verified by multi-compartment analyses, this extremely powerful mixing effect results in an almost fully homogeneous distribution of the CO₂ both inside and outside the missile shield.

5.3 Recombiners or Igniters

Supplementary to the PAD System, equipment for reducing the H₂ concentration in the medium term (e.g. down to < 4% by vol. within 24 h) is required inside the containment/confinement. For this purpose, PARs (such as those shown in Fig. 1) have been developed and qualified and are already in service in several PWR and VVER plants (Fig. 2). When used in combination with a PAD System, significantly fewer recombiners can be installed due to the fact that turbulent combustion will not occur and AICC will be limited to acceptable values. For example, the number of 42 or more recombiners proposed for plants using recombiners alone for hydrogen control can be cut down to less than 20 (e.g. 10).

The same applies in principle for igniters; i.e. due to the prevention of significant flame accelerations leading up to turbulent combustion, the large number of igniters that would otherwise be necessary can be reduced to, for example, 10.

But because the possible special H₂ accumulation and redistribution situation during combustion - as shown in the analysis for a large dry containment (Fig 7 and 8, nearly stoichiometric mixture inside missile shielding) - it seems to be clearly favorable to prefer the combination with recombiners to solve the problems.

6 Prototype Test

The objectives of the prototype test of the semi-passive PAD system were as follows:

- Verification of the functional capability of the new evaporation system
- Optimization of the major components including the high-speed jet injectors.

For this purpose, a mock-up of the system incorporating full-scale injection equipment was tested and optimized (see Fig. 11).

The unit was tested under operation at pressures varying between 15 and 40 bar, evaporation rates ranging from 0.5 to 1.5 kg/s and superheated temperatures of 0 to 40°C, etc., and the process as well as the major components were optimized.

The following results were obtained:

- Heat transfer of up to 0.4 MW for evaporation of 1.5 kg/s CO₂ could be well controlled
- Complete evaporation of all of the liquid CO₂ was achieved in ≤ 15 minutes
- The high-speed jet injectors were operated at suction flows equivalent to 5 to 10 times the CO₂ motive flow and with jet ranges of > 30 m
- The overall process configuration was greatly simplified by maximizing the use of passive components.

6.1 Test Results

High gaseous CO₂ injection rates of, for example, 30 kg/s or more can

- be easily achieved by connecting the tested full-scale components in parallel, despite the high thermal outputs of e.g. 10 to 20 MW required for direct evaporation of the CO₂
- be supplied after selecting appropriate gas pressures and velocities through piping of relatively small diameters (e.g. 100 to 150 mm)
- be distributed almost completely homogeneously inside the containment using high-speed jet injection and mixing.

7 Results of Multi-Compartment Analysis

7.1 General

The multi-compartment analysis will not be described in detail in this paper since the main focus of this publication is to present the new hydrogen control concept itself.

Hence, only a brief description of the analysis is given in the following in order to explain those results which are of significance for evaluating the efficiency of the hydrogen control system.

The analysis was based on e.g. for the geometry of a Siemens PWR containment with a total volume of approximately 70,000 m³. In order to be able to also adequately model local mixture compositions, the containment was subdivided into 33 zones and then analyzed using the multi-compartment code WAVCO.

Based on the results of several studies, a 50-cm² pipe break accompanied by a high H₂ release rate was selected, as mentioned in Section 2.3 above, as an enveloping scenario and then analyzed for case a) and case b) (incl. concrete-melt interaction).

7.2 Results for Case a)

Injection of 30 kg/s CO₂ inside Missile Shield

Pressures and temperatures: The pressures and temperatures determined for the model zones are close to each other.

CO₂ injection causes the maximum pressure to rise by approx. 0.3 bar to 3.7 bar which is negligible in comparison with the approach using recombiners alone.

Ignitability:	The concentration time histories are shown in individual ternary diagrams.
42 recombiners:	The area of fast deflagrations is reached in various zones as shown, for example, in Figures 7 and 8 (left).
20 recombiners + 50 Mg CO ₂ :	As shown in Figures 7 and 8 (right), the combustion regime only touches the area of slow deflagrations in a few zones. Otherwise inert conditions are achieved. In the reactor cavity which is not sufficiently supplied with CO ₂ the atmosphere reaches the transition between slow and fast deflagration.

7.3 Results for Case b) (incl. MCCI)

Compared to case a), in-vessel H₂ production in case b) is low. However, molten-core/concrete interaction (MCCI) causes the total H₂ release to be increased by 650 kg.

This analysis was based on CO₂ injection rates of 12 kg/s inside the missile shield and 18 kg/s outside the missile shield. Hydrogen control was, however, again assumed to be provided using 20 recombiners and 50 Mg of CO₂.

The results of the WAVCO analysis are summarized in Figure 9. In this case it can be demonstrated that combustion does not occur, even locally (with the exception of combustion inside the reactor cavity).

Hydrogen reduction is performed using a reduced number (20) of catalytic recombiners.

7.4 Evaluation of Results

The performed WAVCO analyses revealed that, when applying the ignition limits cited in the literature:

- effective post-accident CO₂ dilution can be achieved within about 20 minutes by injecting 50 Mg of CO₂, primarily inside the missile shield
- local turbulent hydrogen combustion can be prevented - with the exception of the reactor cavity with its walls designed for high differential pressure loading - even at high hydrogen release rates of several m³/s, as would be encountered with slumping or reflooding, etc.

8 System Design for Multiple-Unit Plants

The system design concept presented herein is a highly suitable alternative, particularly in terms of cost, for solving hydrogen control problems at multiple-unit plants. It yields significant cost benefits over systems comprising only either recombiners or igniters for the following reasons:

- Several plant units can be served by only one central PAD system
- Despite the high injection rates, the motive pressures are so high that, even over large distances, piping of only relatively small diameters (e.g. 100 to 150 mm) is required
- Additional components such as PARs or igniters can be reduced in number to, for example, 10 per plant unit
- The temperature range of the injected CO₂ (> 0 °C) allows existing piping to be used for injection
- The time and effort required for performing inservice inspections can be considerably reduced.

A system configuration for a multiple-unit plant is shown schematically in Figure 12.

9 Further Studies

In order to verify the ignition limit and the concentration of the various combustion regimes taken from the literature, it is recommended that experimental work be performed on large-scale models in order to properly determine scaling effects. In addition, multi-compartment code analyses based on a smaller number of recombiners (e.g. 10) are recommended in order to further optimize the hydrogen reduction system.

10 Summary

The development work and analyses that have been carried out show that, even when unfavorable scenarios involving the release of very large quantities of hydrogen and accumulation situations are considered, the new semi-passive PAD system used in combination with passive recombiners enables the following to be achieved:

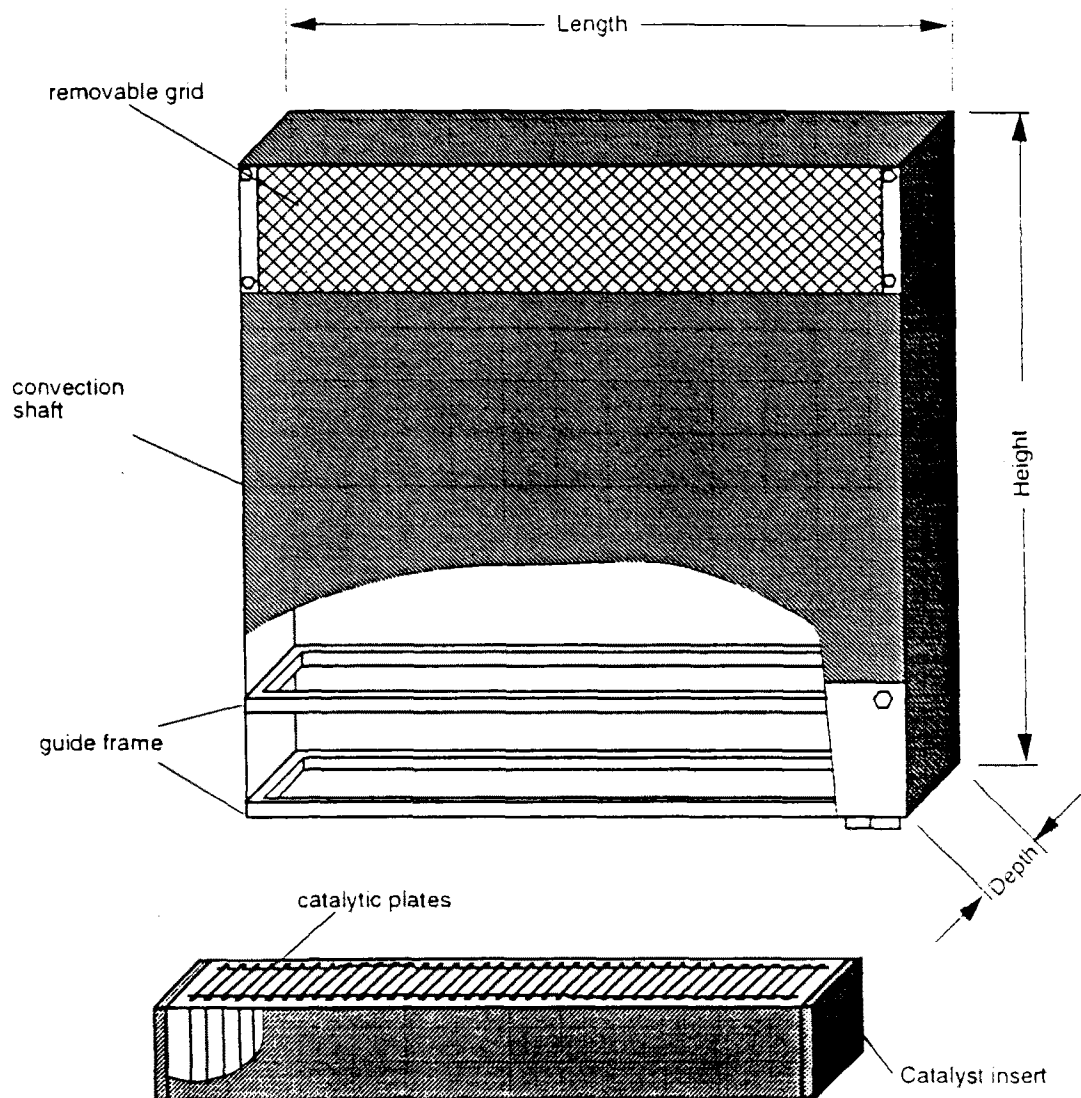
- A sufficient quantity of CO₂ can be injected into the containment
- Fast flame propagation can be ruled out, also for temporary steam inerted critical stoichiometric mixtures
- The resulting thermal and pressure loads are low and can thus be safely accommodated
- Problems associated with thermal shock are avoided
- Gaseous injection allows existing containment penetrations to be used, which leads to a corresponding minimization of costs as well as inservice inspection effort
- Combination of the process with recombiners allows the number of these components to be kept to a minimum

- The installation of a single central PAD system at multiple-unit plants yields considerable cost savings
- High-speed jet injection results in almost completely homogeneous mixing of the CO₂ with the containment atmosphere before significant quantities of hydrogen are released.

11 References

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- 8 B.Regenscheidt, Movement of air in air-conditioned rooms, Kältetechnik 11/59

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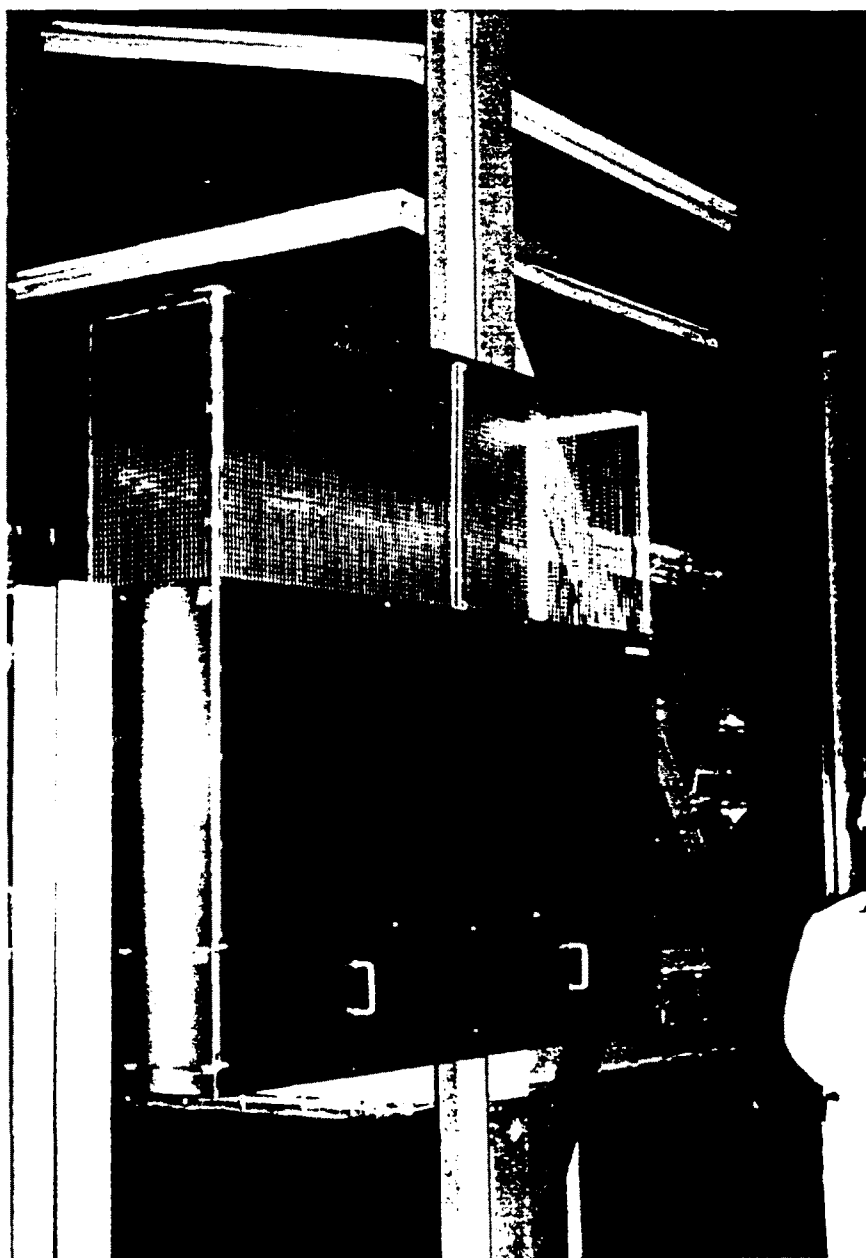


Available Sizes:

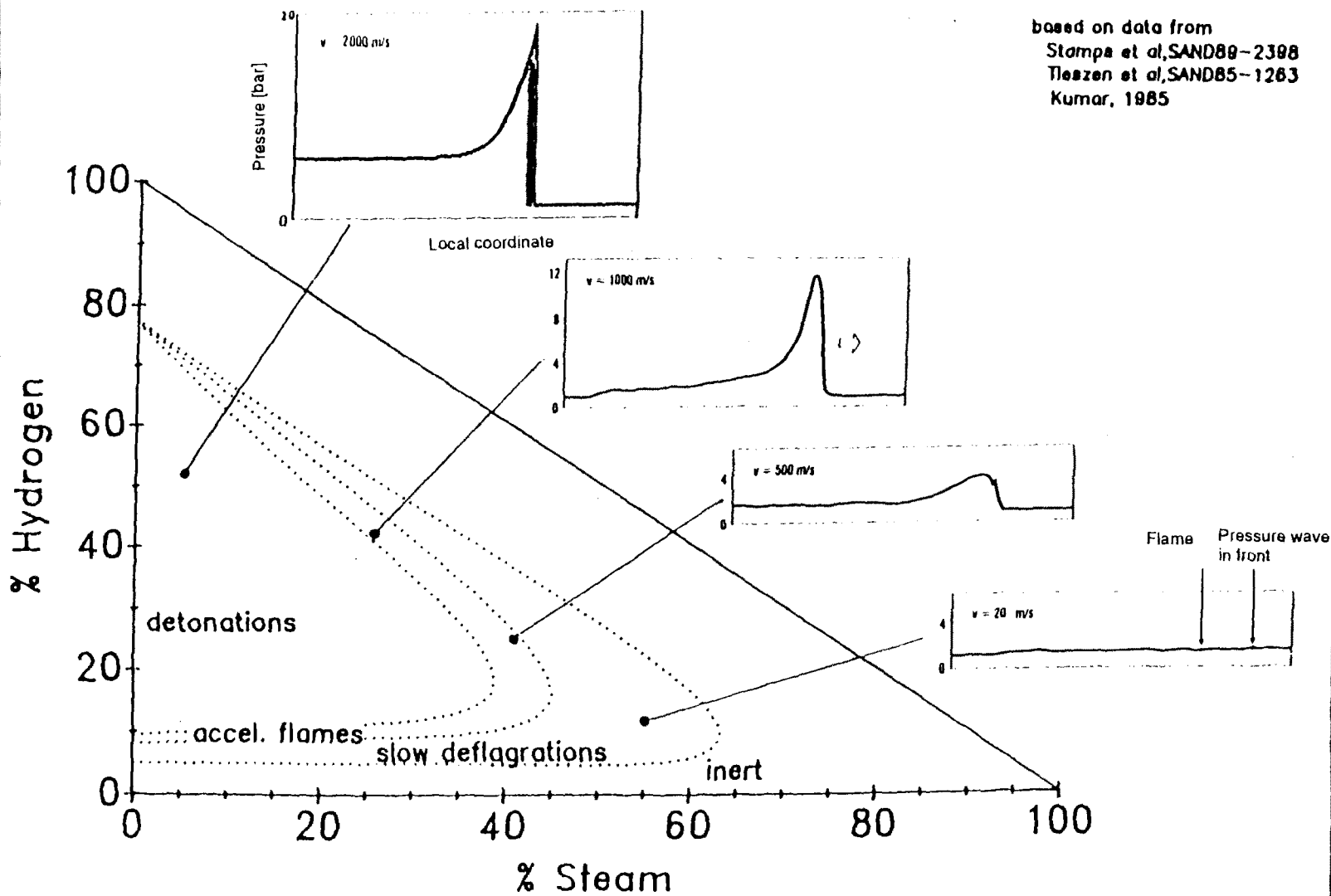
Type	Length (mm)	Depth (mm)	Height (mm)
FR90/1-320	370	166	1000
FR90/1-960	1010	166	1000
FR90/1-1500	1550	326	1400

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Passive Autocatalytic Recombiner (PAR) TYPE FR90/1-1500 installed in PWR



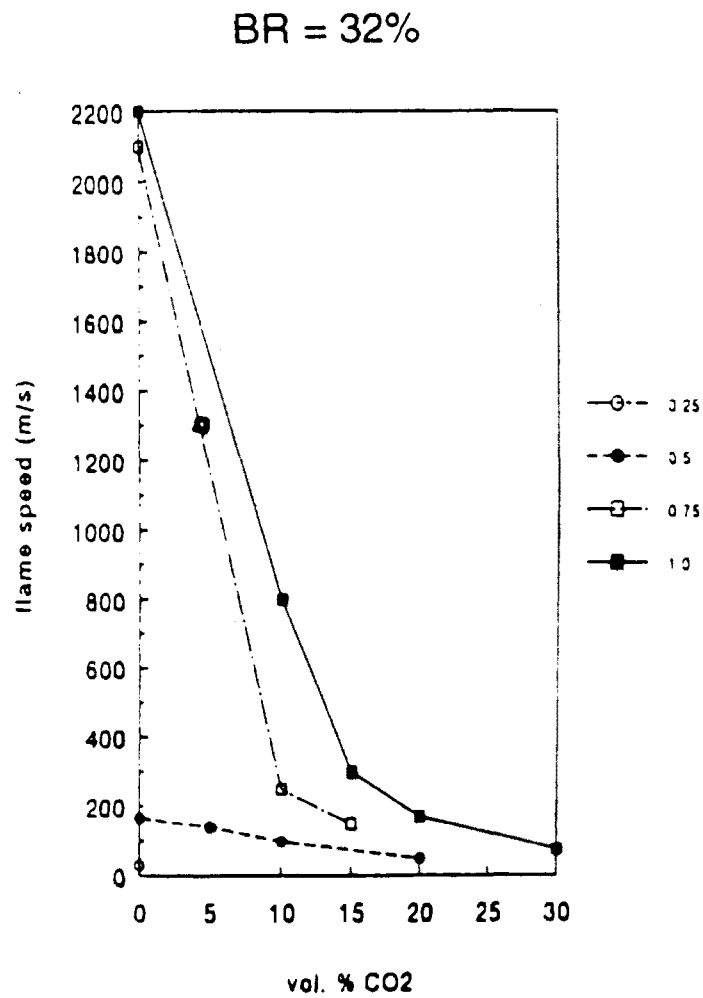
based on data from
Stampa et al, SAND89-2388
Tleszen et al, SAND85-1263
Kumar, 1985



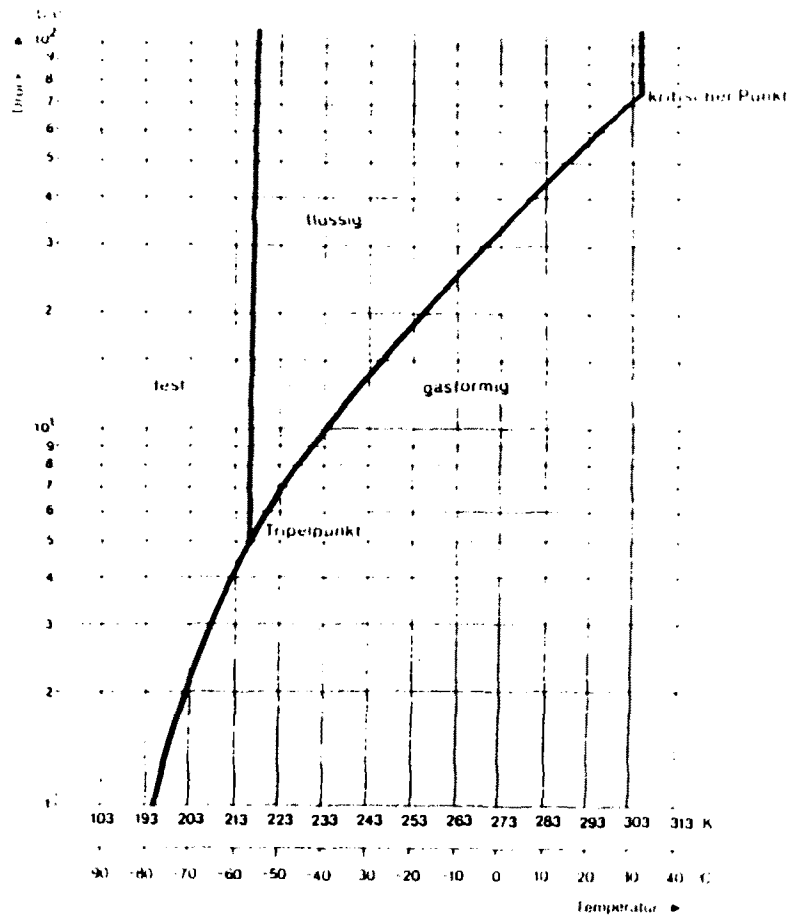
Combustion Regimes at 373 K

Figure 3

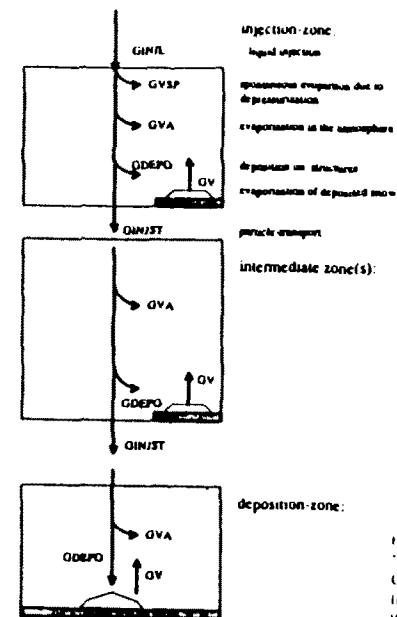
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Flame Speed as a Function of CO₂-Concentration and Degree of Restriction



Phase Diagram CO₂



Injection of liquid CO₂ - Simulation model

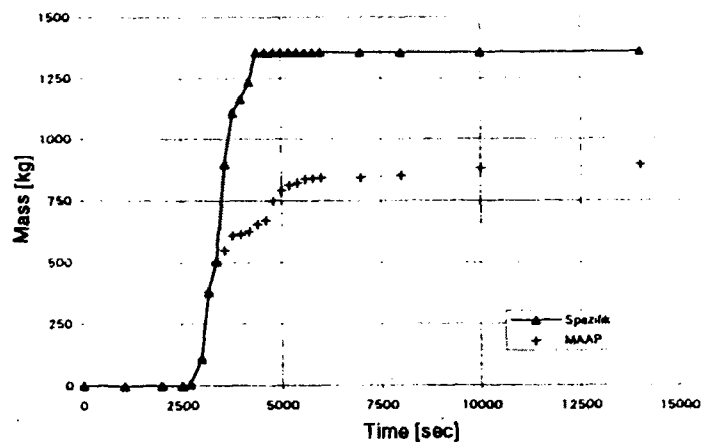
H. Karwal, P. Stolze
"Mitigation of Hydrogen
Challenges by Post-Accident
Inertisation"
Workshop 29.9.94 Bergische
Universität GH Wuppertal

Result

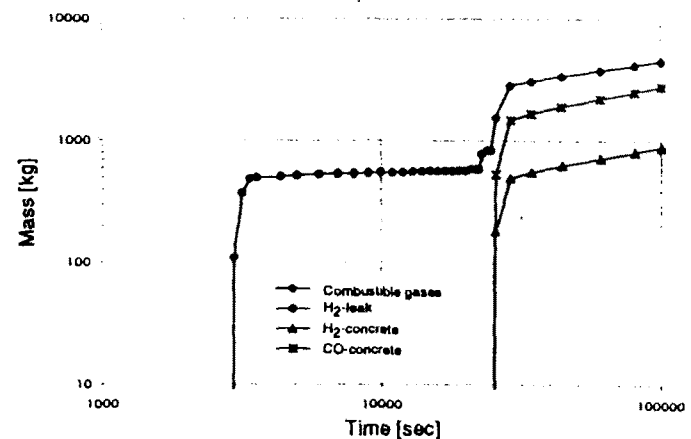
approx. 50 % cold gas
approx. 50 % solid particles per mass

CO₂ - Simulation Model

Figure 5



Release of steam and hydrogen into containment for case a

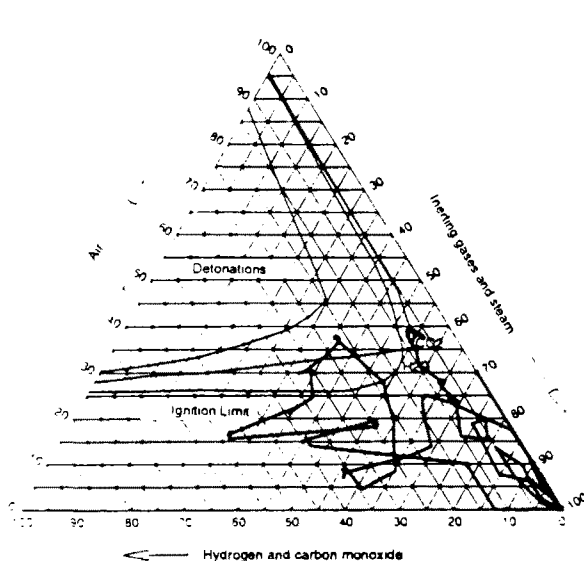


Release of inert gases and combustible gases into containment for case b

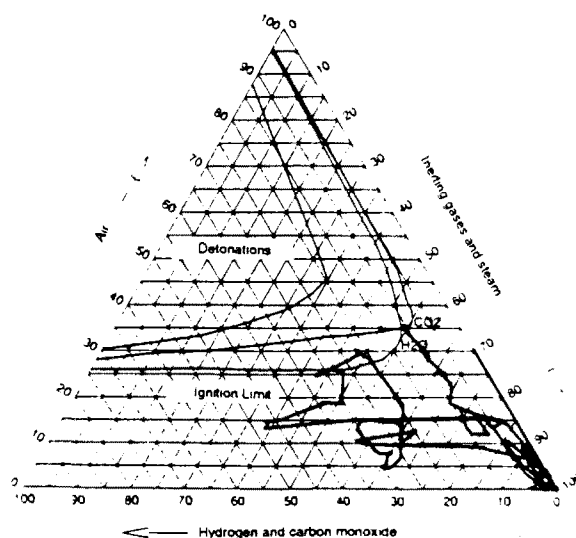
Release of Gases and Steam into Containment

Figure 6

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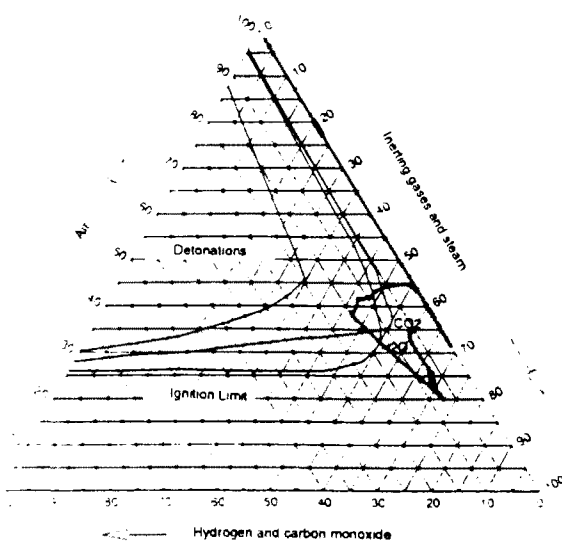


Case 1: 42 recombiners without CO₂-injection

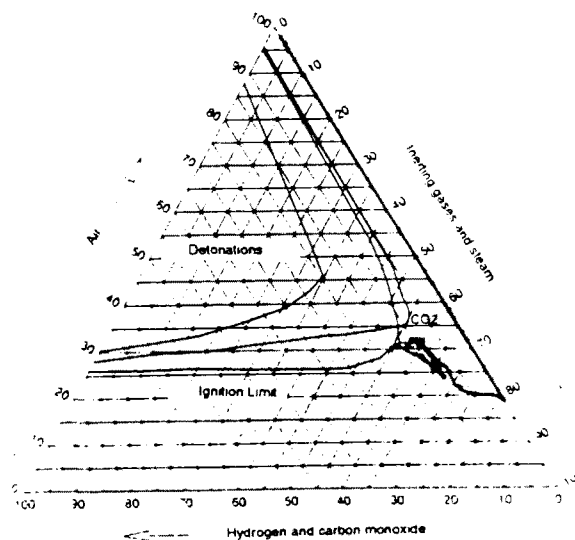


Case 4: 20 recombiners with CO₂-injection
inside missile shield only

Zone 4 - Loop 2 cold



Case 1: 42 recombiners without CO₂-injection



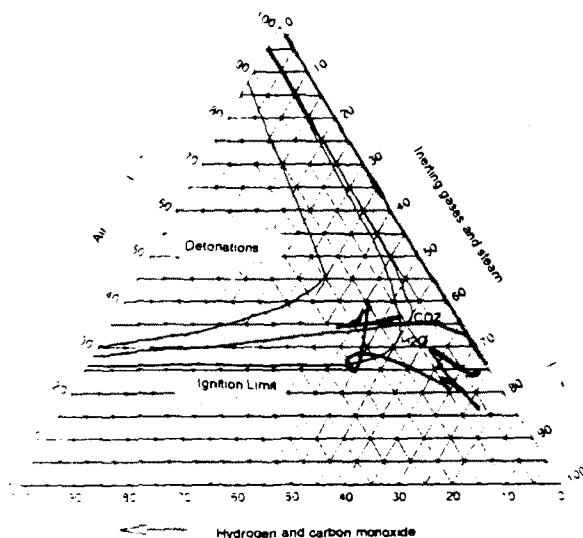
Case 4: 20 recombiners with CO₂-injection
inside missile shield only

Zone 8 - RHR-system 1 + 2

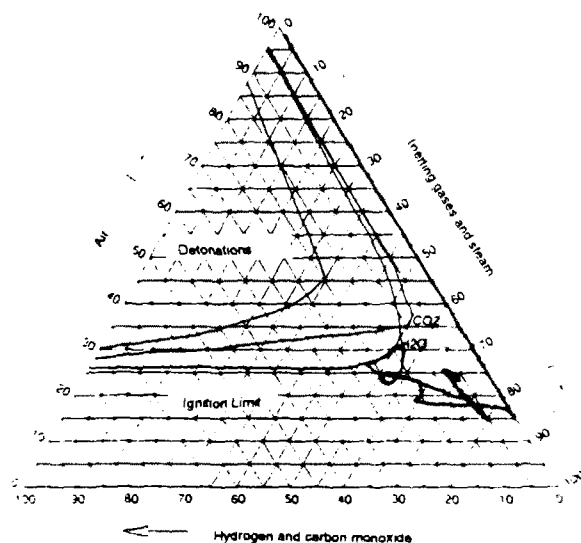
**Composition of Containment Atmosphere in Ternary
Diagrams Based on Different Hydrogen Control Measures**

Figure 7

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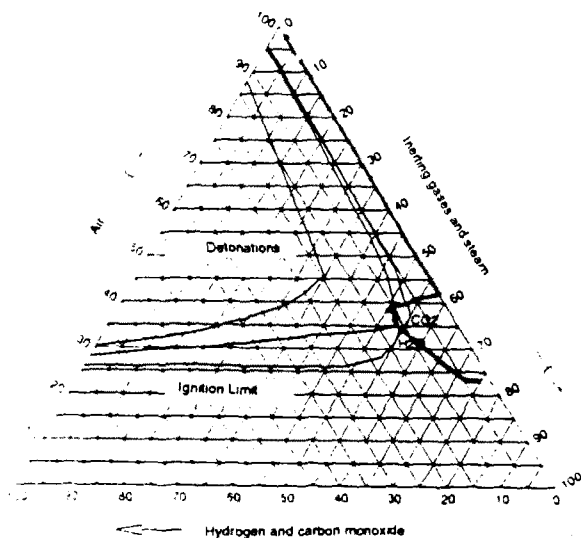


Case 1: 42 recombiners without CO₂-injection

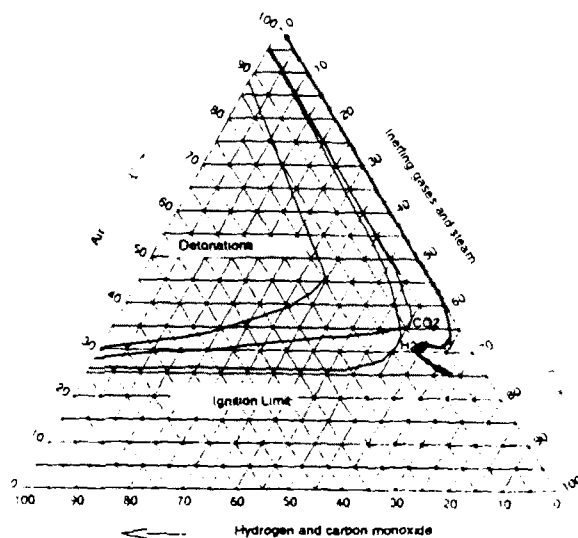


Case 4: 20 recombiners with CO₂-injection
inside missile shield only

Zone 12 - Steam generators 1+2, upper compartments



Case 1: 42 recombiners without CO₂-injection



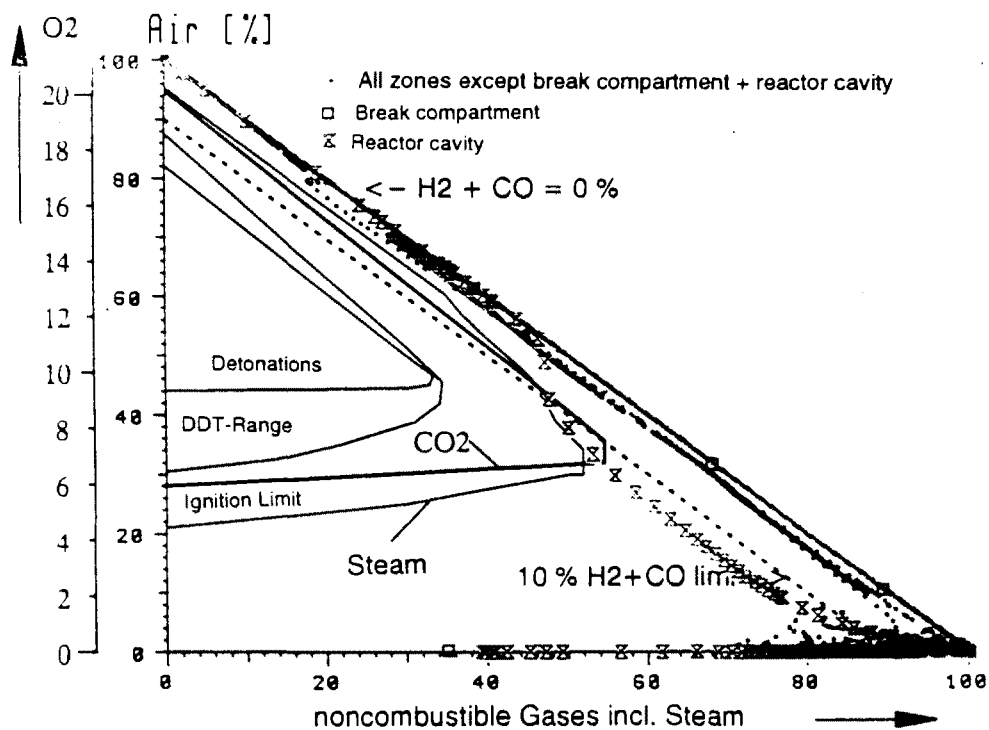
Case 4: 20 recombiners with CO₂-injection
inside missile shield only

Zone 33 - Containment dome

**Composition of Containment Atmosphere in Ternary
Diagrams Based on Different Hydrogen Control Measures**

Figure 8

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$V=69537 \text{ m}^3 / 20 \text{ recombinators}$
 $50 \text{ cm}^3 \text{ break in cold-leg reactor coolant line}$

Composition and Combustibility of Containment Atmosphere

Figure 9

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Schematic Representation of Semi-Passive PAD-System

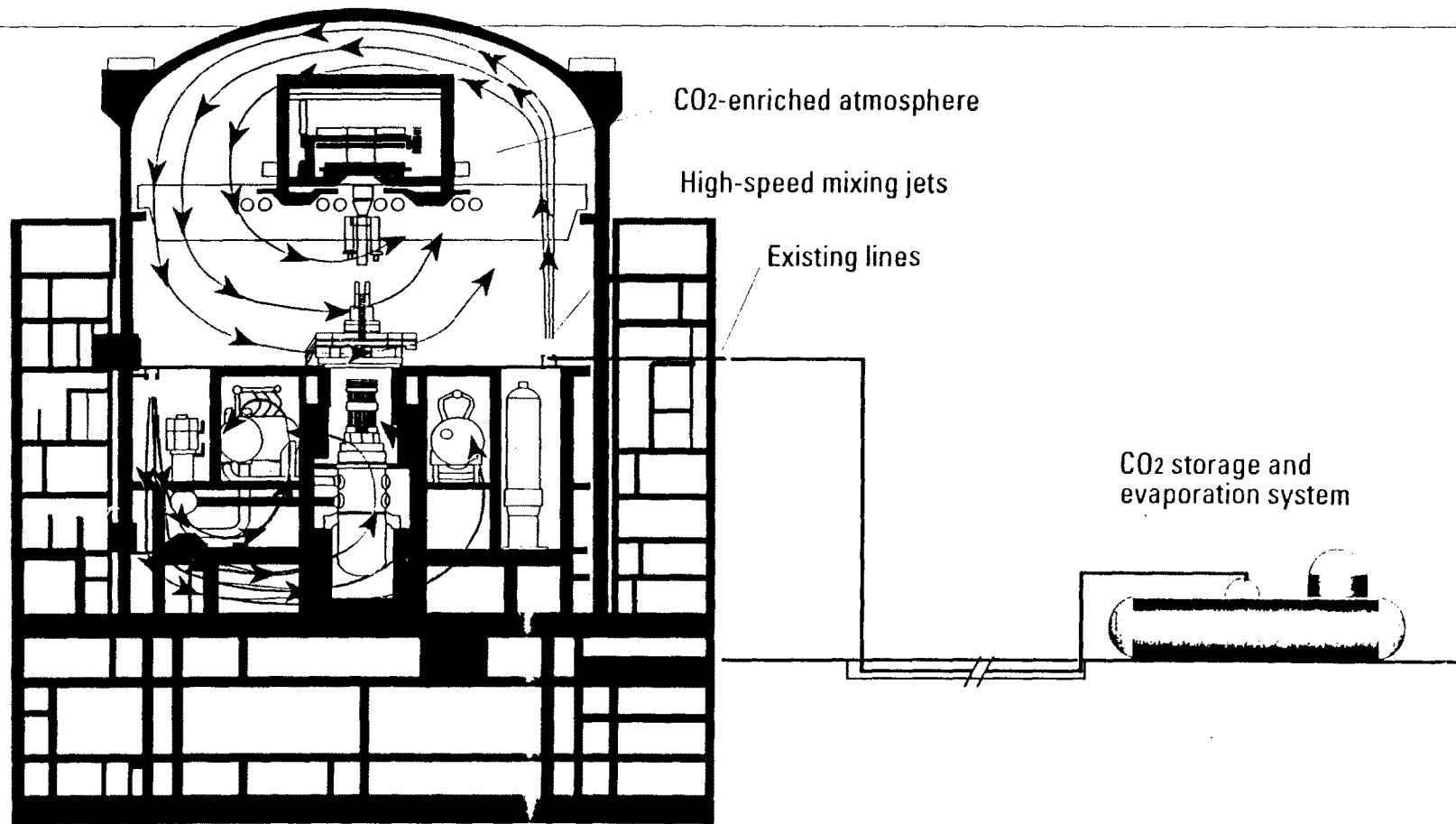
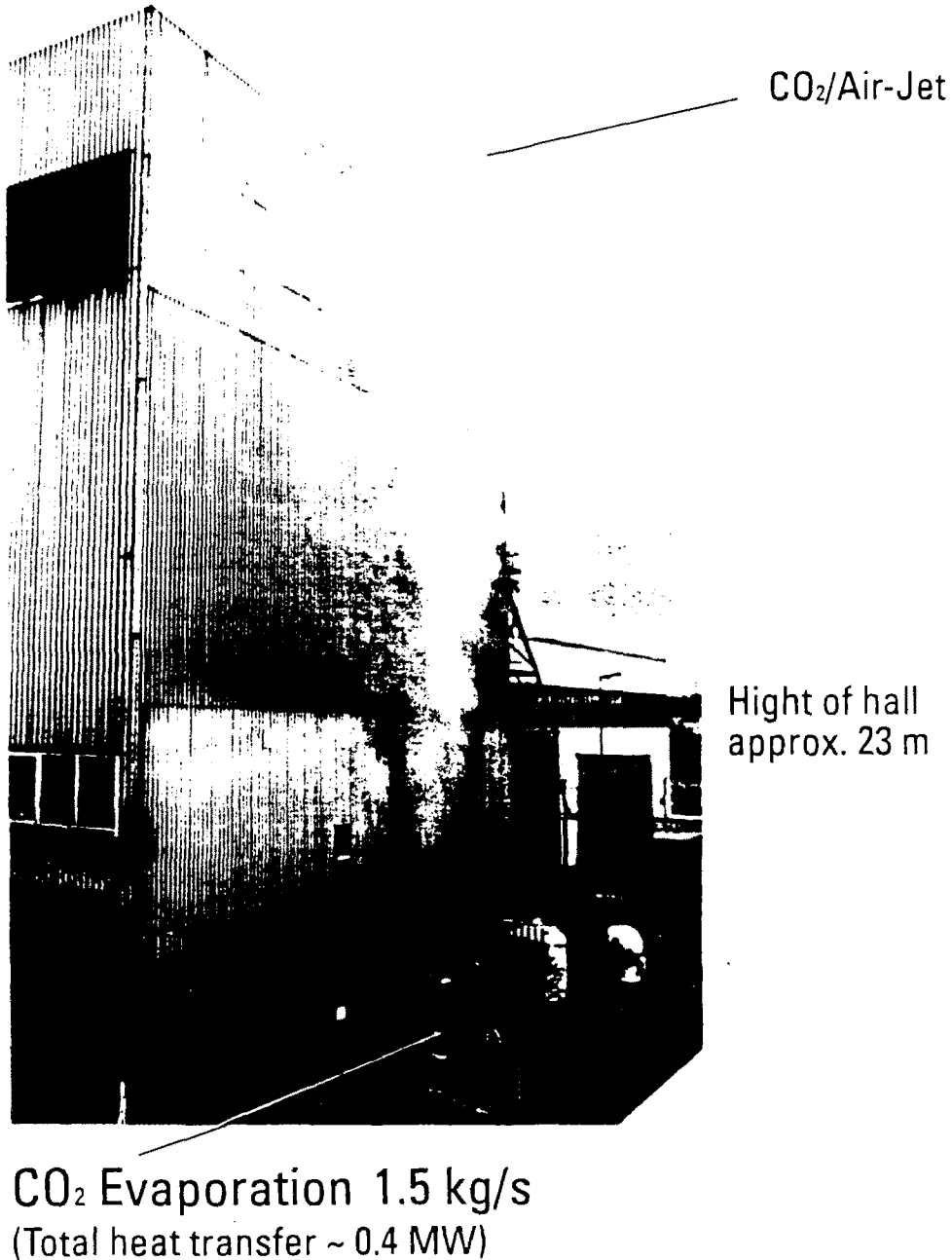


Fig. 10

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Prototypetest Semi-passive PAD-System



Central Semi-Passive PAD System for Multiple-Unit Power Plant

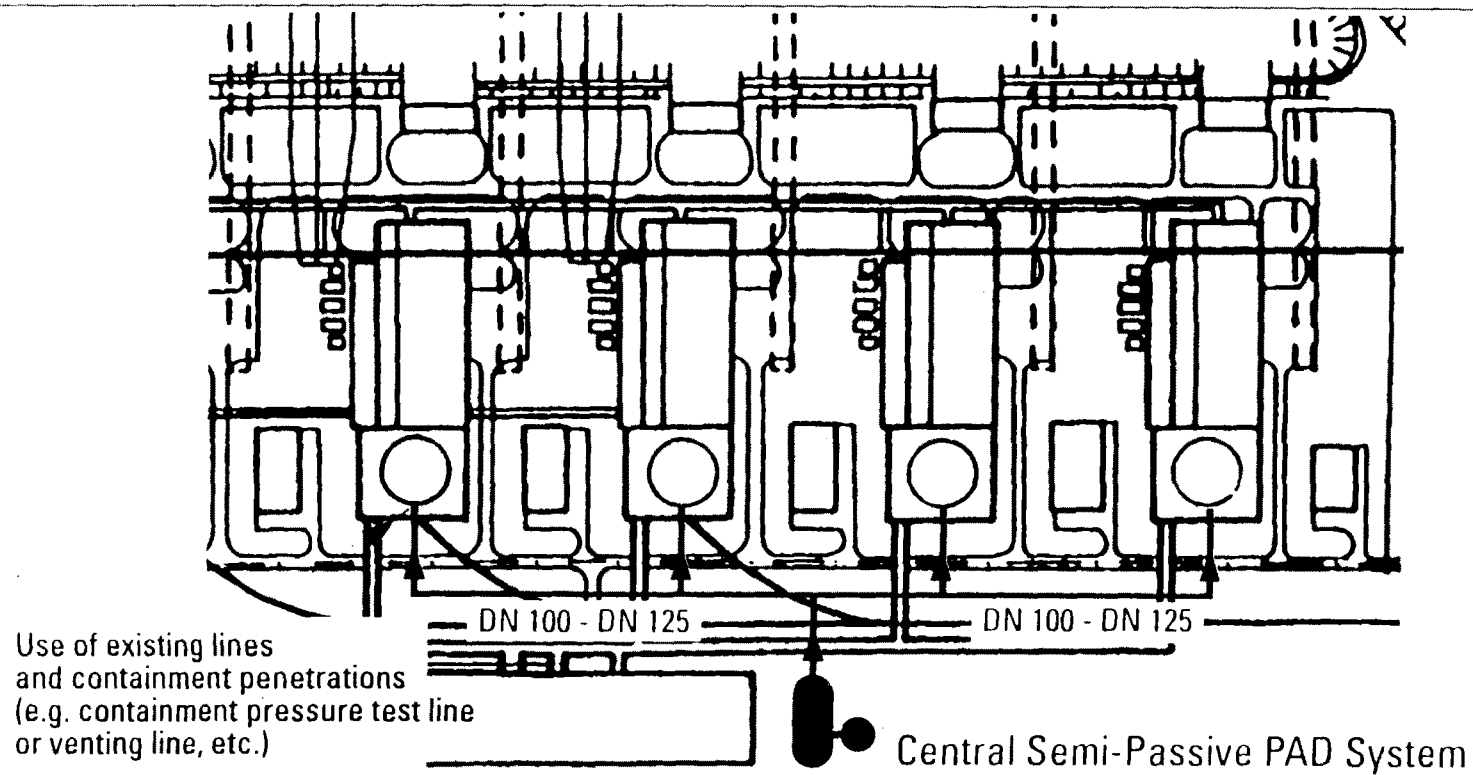
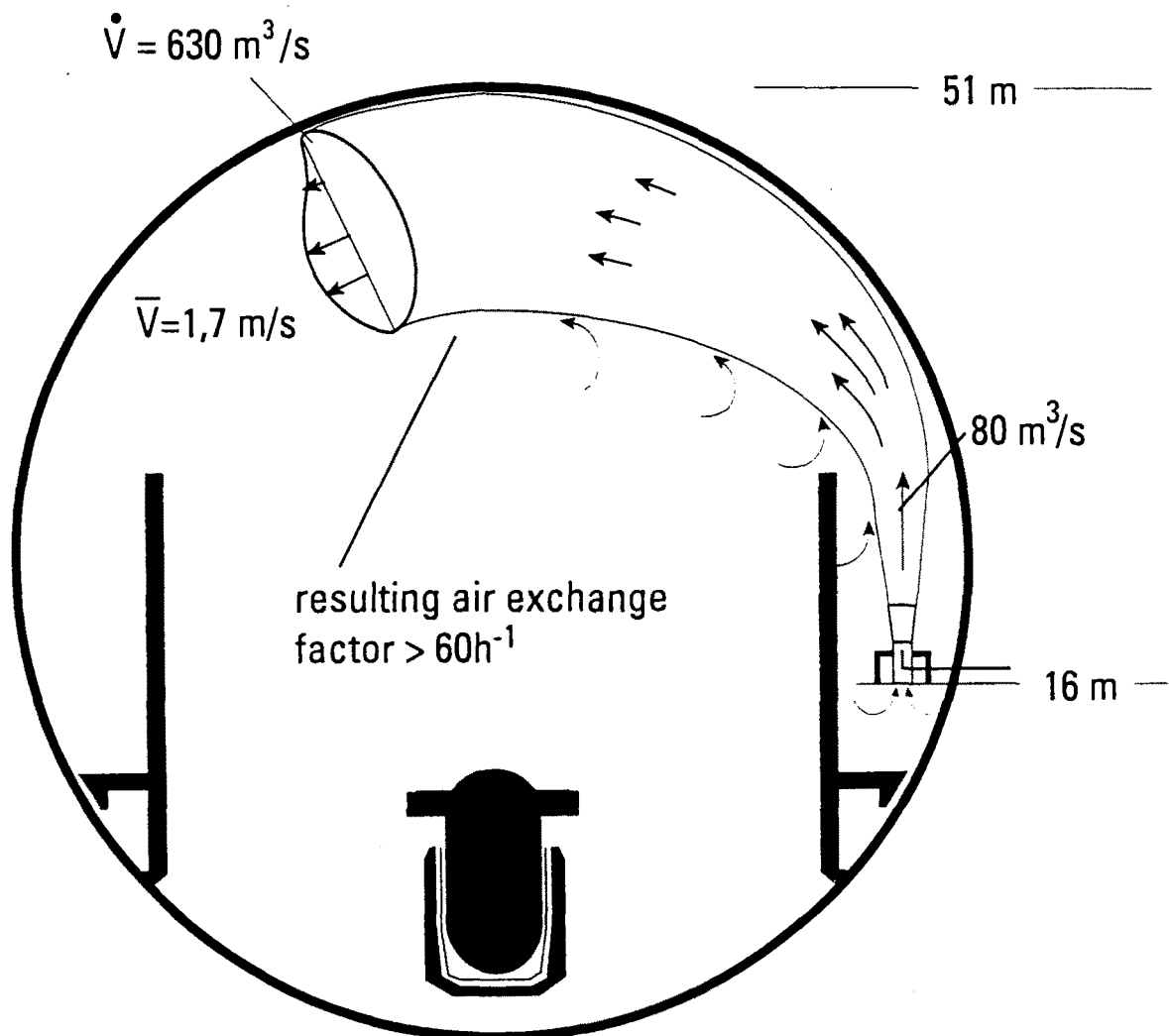


Fig. 12

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Schematic Representation of Hightspeed mixing jet

Example: Mixing jet location outside missile shielding
CO₂-injection rate: 18 kg/s



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4.4 PERFORMANCE TESTING OF PASSIVE AUTOCATALYTIC RECOMBINERS (PARs)

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1. Abstract

Passive autocatalytic recombiners (PARs) have been under consideration in the U.S. as a combustible gas control system in advanced light water reactor (ALWR) containments for design basis and severe accidents. PARs do not require a source of power. Instead they use palladium or platinum as a catalyst to recombine hydrogen and oxygen gases into water vapor upon contact with the catalyst. Energy from the recombination of hydrogen with oxygen is released at a relatively slow but continuous rate into the containment which prevents the pressure from becoming too high. The heat produced creates strong buoyancy effects which increases the influx of the surrounding gases to the recombiner. These natural convective flow currents promote mixing of combustible gases in the containment. PARs are self-starting and self-feeding under a very wide range of conditions. The recombination rate of the PAR system needs to be great enough to keep the concentration of hydrogen (or oxygen) below acceptable limits.

There are several catalytic recombiner concepts under development worldwide. The USNRC is evaluating a specific design of a PAR which is in an advanced stage of engineering development and has been proposed for ALWR designs.

Sandia National Laboratories (SNL), under the sponsorship and the direction of the USNRC, is conducting an experimental program to evaluate the performance of PARs. The PAR will be tested at the SURTSEY facility at SNL. The test plan currently includes the following experiments:

- Experiments will be conducted to define the startup characteristics of PARs (i.e., to define what is the lowest hydrogen concentration that the PAR starts recombining the hydrogen with oxygen).
- Experiments will be used to define the hydrogen depletion rate of PARs as a function of hydrogen concentration.
- Experiments will be used to define the PAR performance in the presence of high concentrations of steam.

2. PAR Description

Figure 1 shows the PAR test module. The PAR test module is a scaled version of the prototype PAR [EPRI 1993]. The prototype PAR contains two rows of standard catalytic cartridges (44 cartridges per row) and has dimensions of 1 m by 1 m. The test module can be assembled as either a 1/2 scale, 1/4 scale, or 1/8 scale PAR by removing cartridges and using smaller (length) front and back panels. Figure 2 shows a top view comparing the PAR test module at the three scales.

The body of the module holds the catalyst cartridges (or inserts) in a vertical direction and guides the air flow. A vertical flow channel is formed between the cartridges which maintains about a 1 cm spacing between cartridges. These flow channels (along with the PAR body or housing) define the flow area for convection of the heat generated by the heat of reaction. The PAR exit has a chimney with a free cross-sectional area equal to the cross-sectional area through the cartridges. The chimney eliminates downward flow in the PAR and improves the net flow through the PAR to enhance hydrogen removal.

The catalyst is a palladium-coated aluminum oxide pellet with a diameter of about 2-4 mm. Rectangular cartridges (0.45 meter length, 0.01 m wide, 0.20 m tall) are filled with the catalyst pellets. The sides of the cartridges are perforated with many slotted-like openings that allow hydrogen to enter into the cartridge.

3. Test Facility Description

Figure 3 presents a view of the Surtsey vessel and the instrumentation and equipment that will be used in the PAR experiments. The Surtsey vessel is an ASME-approved steel pressure vessel with an internal volume of 90 m³. It has a cylindrical shape with removable, dished heads attached to both ends, and is 3.6 m in diameter by 10.3 m high. The Surtsey vessel has a maximum allowable working pressure of 1 Mpa at 260 K, but has a burst diaphragm installed to limit the pressure in the vessel to less than 0.9 Mpa. It is supported approximately 2 m off the ground by a structural steel framework with its longitudinal axis oriented vertically. A total of twenty 30.5 cm (12 inch) and 61 cm (24-inch) instrument penetration ports exist at six different levels around the perimeter of the vessel. The vessel walls and heads are 3/8 in thick and covered with at least 4 inches of fiberglass insulation, or equivalent material.

The PAR is located at the vessel centerline, with the PAR inlet at about 4.5 m from the vessel dome. At about 1 m below the PAR inlet exist horizontal and vertical I-beams which previously supported a false floor. Figure 4 shows the layout of the PAR and the horizontal I-beams.

Numerous flanges on the vessel have been modified to allow steam, noncondensable gas, water, electrical, and video service into and out of the vessel.

4. Instrumentation, Control, and Data Acquisition

Due to the novelty of the PAR test module and the complexity of the planned tests, the instrumentation and control (I&C) equipment and the data acquisition system have been designed to be very flexible. This will allow changing the test conditions (during the course of a test) based on real-time test results. The test instrumentation is described below.

Four pressure transducers will be used to measure the pressure in the Surtsey vessel. The gas temperature in the Surtsey vessel will be measured with twenty thermocouples installed in two rakes (see Figure 5). The two thermocouple rakes are installed vertically in the vessel; one rake at the vessel centerline (array A) and one rake (array B) located about 0.45 m from the vessel wall. Ten equally-spaced thermocouples (1.0 m spacing) are located on each rake.

Seven thermocouples are installed in the Surtsey vessel steel walls. Five will measure wall temperature, one will measure dome temperature and one will measure floor temperature. In addition, thermocouples will measure the injected oxygen and hydrogen manifold temperature and also the temperature of the inlet steam.

A real-time gas mass spectroscopy (GMS) system will be used to determine the concentrations of nitrogen, oxygen, and hydrogen in the vessel at four sample points. The sample points are the PAR inlet, the PAR outlet, high in the vessel near the dome, and low in the vessel near the floor. To ensure representative samples and to minimize the delay time due to purging sample lines, each line will be purged for ~30 s prior to sampling. This will necessitate a continuous purge of gas out of the vessel. The sample lines and purge rates have been sized to allow no more than a 1.0 % loss (by volume) of gas out of the vessel over the course of a 12 hour test. Since the PAR inlet is the sample point of greatest interest, this point will be selected for every other sample (i.e., PAR inlet, PAR outlet, PAR inlet, dome, PAR inlet, Surtsey floor, PAR inlet, PAR outlet, ...).

The GMS system cannot measure steam concentrations, a dry sample must be presented. In order to achieve this, a condenser and condensate trap (and heated gas inlet lines) have to be installed on each measurement line. This will yield dry-basis gas concentrations; however, to determine wet-basis gas concentrations, the steam fraction must be known. A hygrometer is used to determine the relative humidity and a calculation is performed to determine the steam concentration.

Twenty pre-evacuated 500-cm³ gas grab sample bottles will be used to collect samples from the vessel. Any of the four gas sample points can be selected. These samples will be used as an independent method to determine gas composition. The sample times are chosen based on the expected PAR performance and the estimated test duration time. All sample lines were purged prior to filling the gas grab bottles to ensure representative samples. All of the gas samples will be analyzed after the test using gas mass spectroscopy.

A high resolution 1/2" charge-coupled device (CCD) color camera and an infrared (IR) camera are used to view the PAR in the event of a deflagration.

The hydrogen and oxygen gas will be supplied to the vessel from separate manifolds. For every mole of hydrogen that is added, one-half mole of oxygen is also added. Mass flow controllers are used to provide precise metering of the hydrogen and oxygen into the vessel.

Two mixing fans are installed in the vessel located near and on opposite sides of the PAR. One fan points upward and the other points downward.

A PC-based data acquisition system has been designed to control and monitor the course of the test in real-time. Plots of the pertinent parameters provide indication of the PAR heatup and H_2 concentration reduction during the course of the test. Monitors are used to show the vessel gas concentrations, oxygen and hydrogen total flow and flow rates, gas temperatures from the two vertical arrays, vessel pressure, and the velocity of the gas at the PAR inlet.

5. Initial Test Conditions

Six tests are used to determine the PAR performance (PAR-1 through PAR-6). The test matrix is summarized in Table 1.

The goal of the first three tests (PAR-1 through PAR-3) is to determine the minimum concentration at which the PAR begins to recombine. Continuous injection of H_2 and O_2 is required to obtain a steady state performance at the targeted hydrogen concentration in tests PAR-4 through PAR-6. The continuous injection of relatively cold gases may cause steam in the vessel to condense (which may be compensated for by the production of steam by the PAR). Steam preheating of the injected gases may be required if steam condensation is excessive.

The hydrophobic coating is destroyed when the PAR reaches a temperature of above 200°C . The PAR is estimated to reach the 200°C at 2% H_2 in a dry air and hydrogen environment and at 1.4% H_2 in an air, steam, hydrogen environment. Tests PAR-1, PAR-2, and PAR-3 will be performed at hydrogen concentrations that will not destroy the hydrophobic coating. The PAR will be located at the vessel centerline in all tests.

The following describes each test in detail.

PAR-1

PAR-1 will determine the minimum hydrogen concentration at which the PAR starts in an air atmosphere. The PAR is configured as a 1/2 scale PAR module (~0.5 m in length with 44 catalytic cartridges installed) with the ~0.5 m tall additional chimney attached.

The Surtsey vessel will be pressurized to 31 psia with bottled air (20.9 % O_2) at ambient temperature ($\sim 20^\circ\text{C}$). Hydrogen will be injected in increments of 0.2% H_2 . About every 1/2 hour, hydrogen and oxygen will be injected to make up that amount that was consumed by the PAR (determined by the real-time gas sampling system) and also increase the volumetric concentration by 0.2 %. It is proposed that the mixing fans be operated only when hydrogen is being injected (30 s pulse) in the 0.2 % increments. Hydrogen will be added until

the concentration reaches 2.0 %. The 2.0 % limit is based on maintaining the PAR temperatures less than 200°C (assuming an 80°C temperature increase for every 1 % hydrogen).

PAR-2

PAR-2 is a repeat of PAR-1 to verify the minimum hydrogen concentration at which the PAR starts.

PAR-3

PAR-3 will determine the minimum hydrogen concentration at which the PAR starts in an air/steam atmosphere. The PAR will be configured as a 1/2 scale PAR module. The Surtsey vessel will be sealed with 12.2 psia of cold air inside. Steam will be injected until a total pressure of 31 psia is achieved (at 102°C). This will provide an initial atmosphere concentration of 50 % air (10.5 % O₂) and 50 % steam. Note that all of the following tests (PAR-4 through PAR-6) will be performed using this air/steam initial mixture. Hydrogen will be injected in increments of 0.1 %. The hydrogen and oxygen will be preheated by the use of a steam injector in separate diffuser/mixer pipes located near the floor of the Surtsey vessel.

PAR-4

The PAR will be configured as a 1/2 scale PAR module. The Surtsey vessel will contain a 50/50 mixture of air and steam at a total pressure of 31 psia. A large amount of hydrogen will be injected very quickly (over a few minutes) to achieve a hydrogen concentration of about 1.4 %. It is estimated that the PAR temperature will not exceed the temperature needed to destroy the hydrophobic coating. Once the hydrogen concentration level reaches 1.4 % inside the Surtsey vessel, the concentration will be maintained at that level by injecting small amounts of hydrogen and oxygen, until the PAR reach a steady-state condition. PAR performance data will be obtained while the PAR is operating at steady state. Hydrogen depletion rate data will be taken for about 4 hours after the injection is stopped.

PAR-5

The PAR will be configured as a 1/8 scale PAR module. The atmosphere concentrations and hydrogen addition will be similar to the PAR-4 test. Depletion rate data will be taken for about 7 hours after the injection is stopped.

PAR-6

The PAR will be configured as a 1/8 scale PAR module. The atmosphere concentrations and hydrogen addition will be similar to the PAR-4 test. Depletion rate data will be taken for about 7 hours after the injection is stopped.

6. Test Results and Preliminary Interpretation

PAR-1, the first test using the 1/2 scale PAR was performed. The Surtsey vessel was sealed and pressurized with air to 30.5 psia at a gas temperature of about 15°C. Hydrogen was added to achieve the target concentrations at the times indicated in Table 2. Table 2 also records the temperature increase of the gas flowing through the PAR and also the maximum temperature of the catalyst pellets. Figures 6, 7, 8 and 9 show the PAR inlet and outlet temperatures, the gas temperature from the Surtsey vessel centerline vertical array, and the gas concentrations as recorded by the real-time gas mass spectrometer system.

The following preliminary conclusions can be made:

- 1) The PAR started recombining the hydrogen with the oxygen after the first hydrogen addition. This was shown by the catalyst and PAR temperature increases, and the decreasing hydrogen concentration.
- 2) The PAR appears to generate a convective flow loop in the Surtsey vessel from the PAR outlet, to the dome, down the Surtsey wall until the flow reaches the height of the PAR inlet, and then the flow returns to the PAR inlet. This was shown by the relatively flat temperatures at the thermocouples located in the array below the PAR. In addition, the hydrogen concentration from the sample point located near the floor showed small decreases, as compared to the other sample points. The mixing fans were turned on at about 1 hour and 15 minutes after the last addition of hydrogen and a vessel average hydrogen concentration was obtained. The average concentration was about 0.7 mole %. This indicated that little depletion occurred in the lower half of the Surtsey vessel.

7. Reference

EPRI ALWR Program (April 8, 1993), "Qualification of Passive Autocatalytic Recombiners for Combustion Gas Control in ALWR Containments."

Table 1. PAR Performance Test Matrix

Test #	Purpose	PAR Scale	Atmosphere
1	Startup & performance	1/2	2 bar air, no steam, .2%, .4%, ...1%, ... 2.0% H_2
2	Startup & performance	1/2	2 bar air, no steam, .2%, .4%, ...1%, ...2.0% H_2
3	Startup & performance	1/2	1 bar air, 1 bar steam, .5,..., 1.3% H_2
4	Scale & performance	1/2	1 bar air, 1 bar steam, continuous injection of H_2 and O_2 until PAR reaches steady state at 1.4% H_2 *
5	Scale & performance	1/8	1 bar air, 1 bar steam, continuous injection of H_2 and O_2 until PAR reaches steady state at 1.4% H_2 *
6	Scale & performance	1/4	1 bar air, 1 bar steam, continuous injection of H_2 and O_2 until PAR reaches steady state at 1.4% H_2 *

*The 1.4% H_2 concentration is a preliminary estimate of the highest H_2 concentration that will not destroy the hydrophobic coating.

Table 2. Preliminary Results of the PAR-1 Experiment

Time (hr, min)	H_2 added (g.moles)	H_2 mole % target	H_2 mole % actual	PAR ΔT ($^{\circ}C$)	PAR T_{max} ($^{\circ}C$)
0:00	16	0.20	0.30	9	39
0:33	16	0.40	0.42	12	43
1:04	24	0.60	0.59	16	60
1:37	32	0.80	0.75	21	75
2:10	48	1.00	0.98	28	100
2:45	112	2.00	1.85	54	182

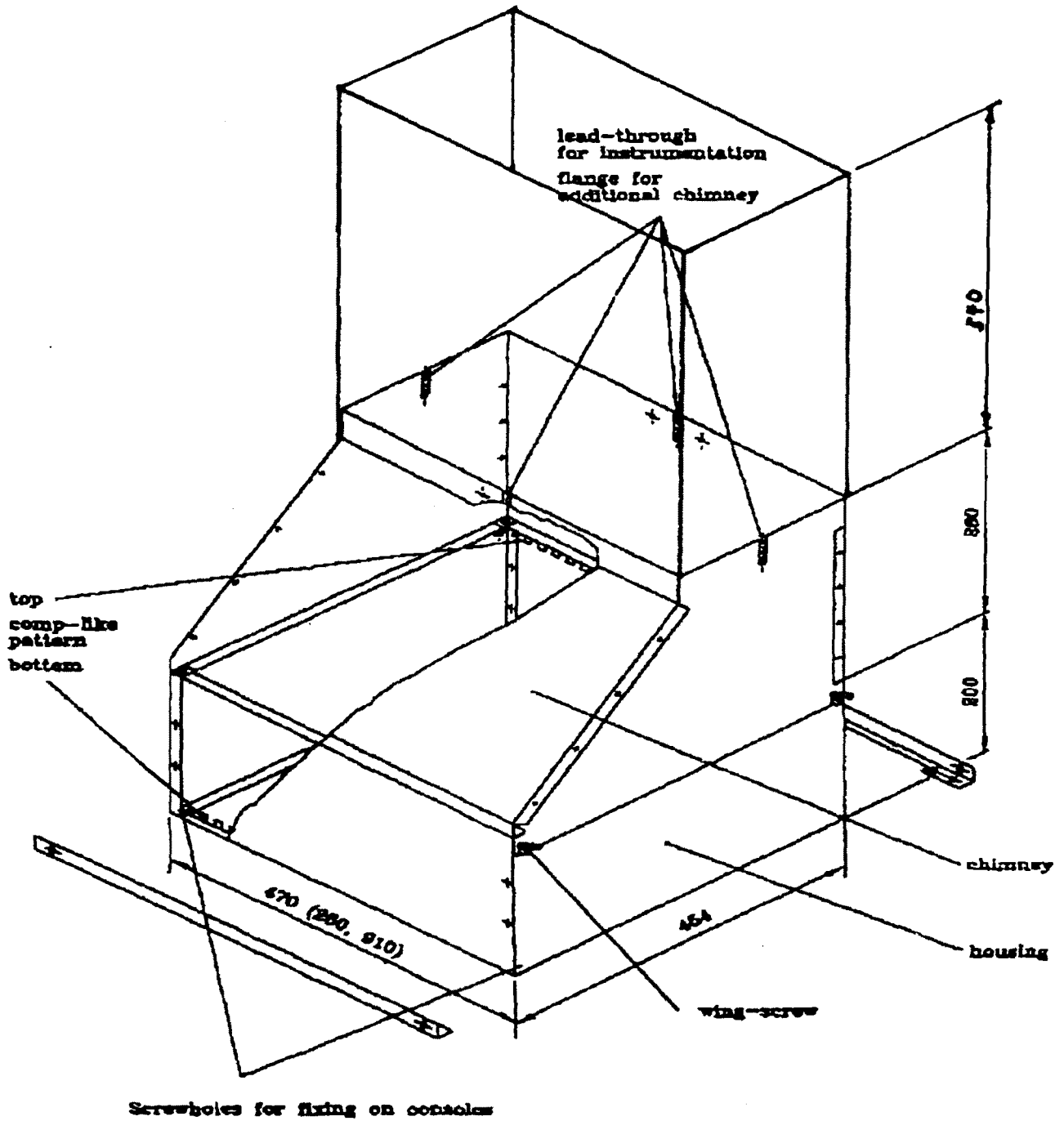
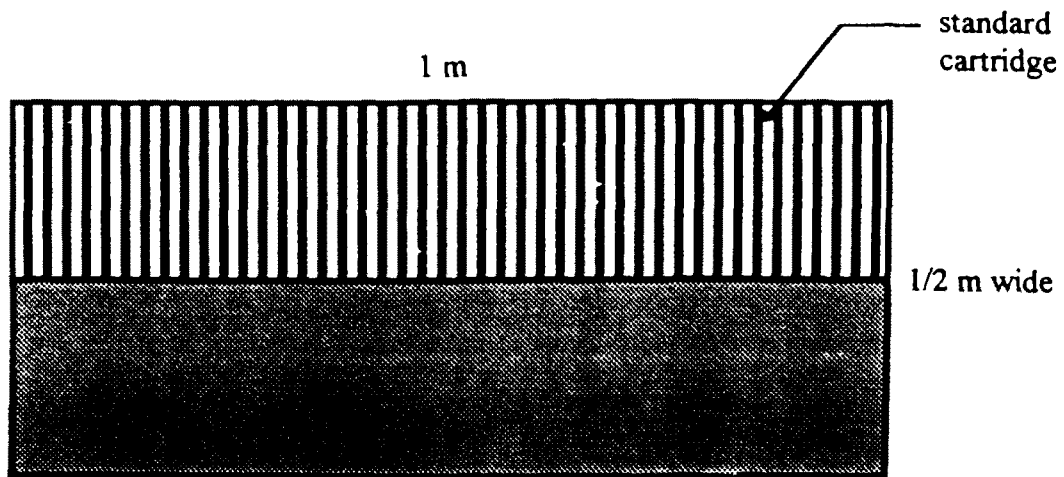
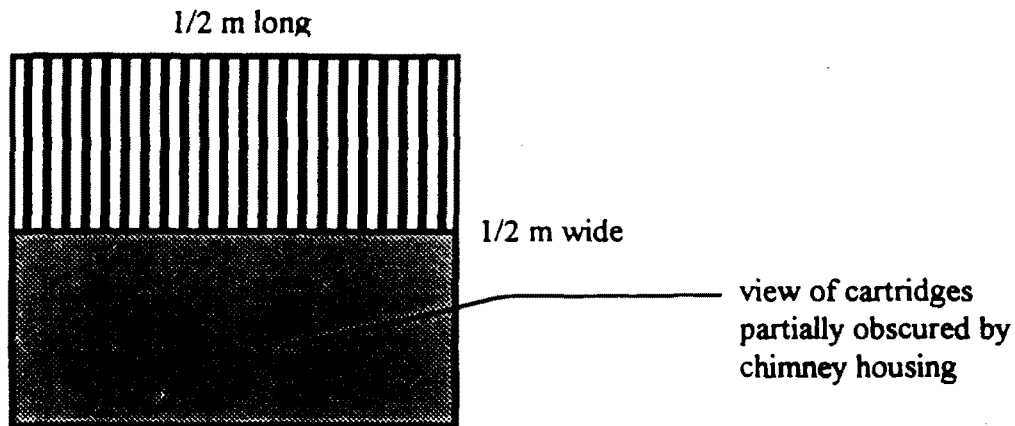


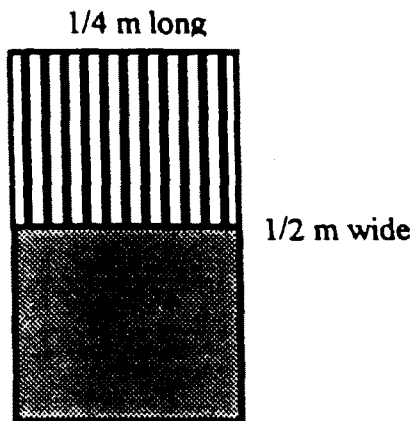
Figure 1. Scaled PAR (dimensions in mm).



**1/2 SCALE PAR
44 STANDARD**



**1/4 SCALE PAR
22 STANDARD**



**1/8 SCALE PAR
11 STANDARD**

Figure 2. Top View of the 1/2, 1/4, and 1/8 Scale PAR Module

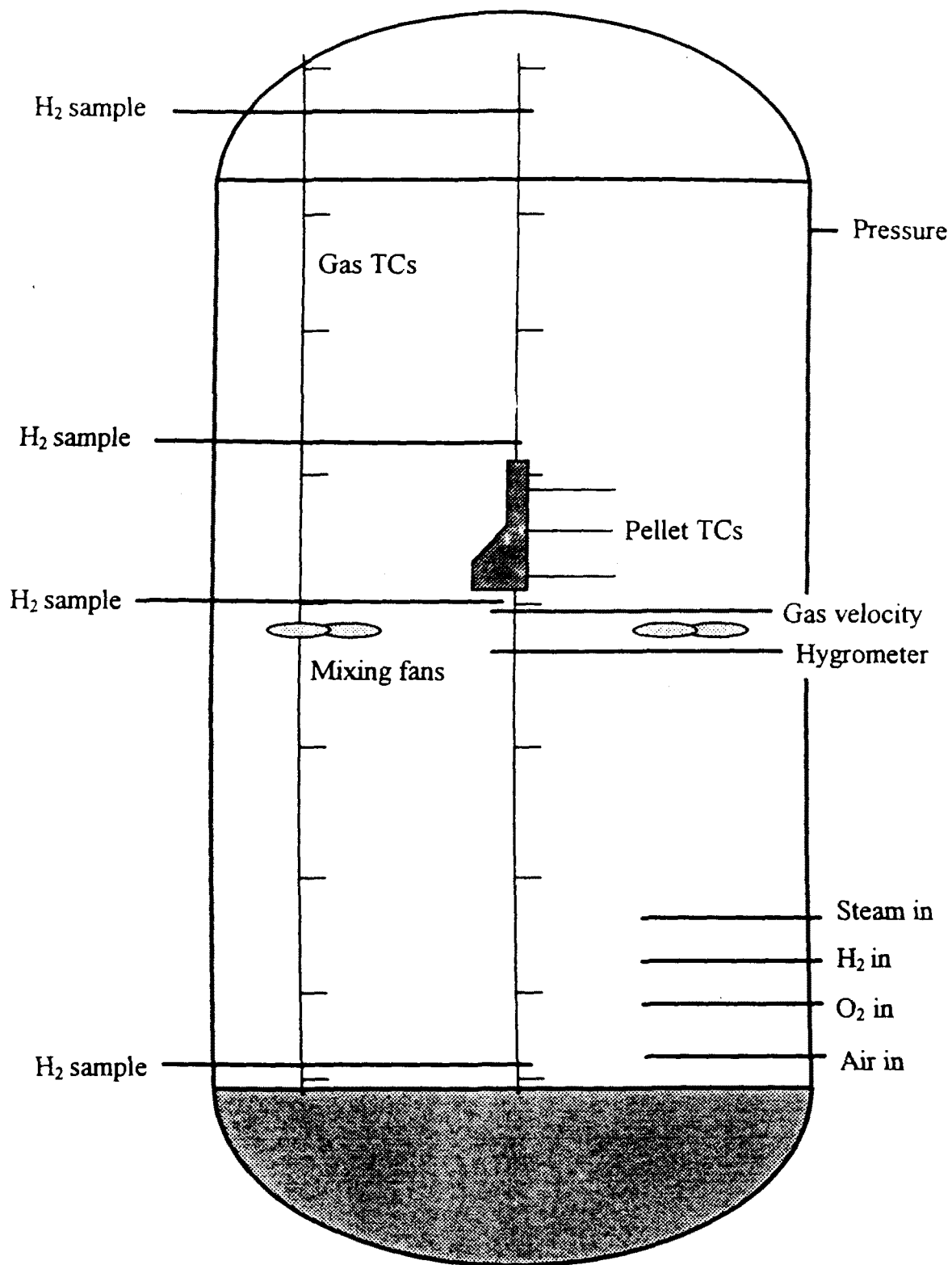


Figure 3. Instruments and controls for the PAR performance tests.
(90 m³ freeboard volume in the Surtsey vessel).

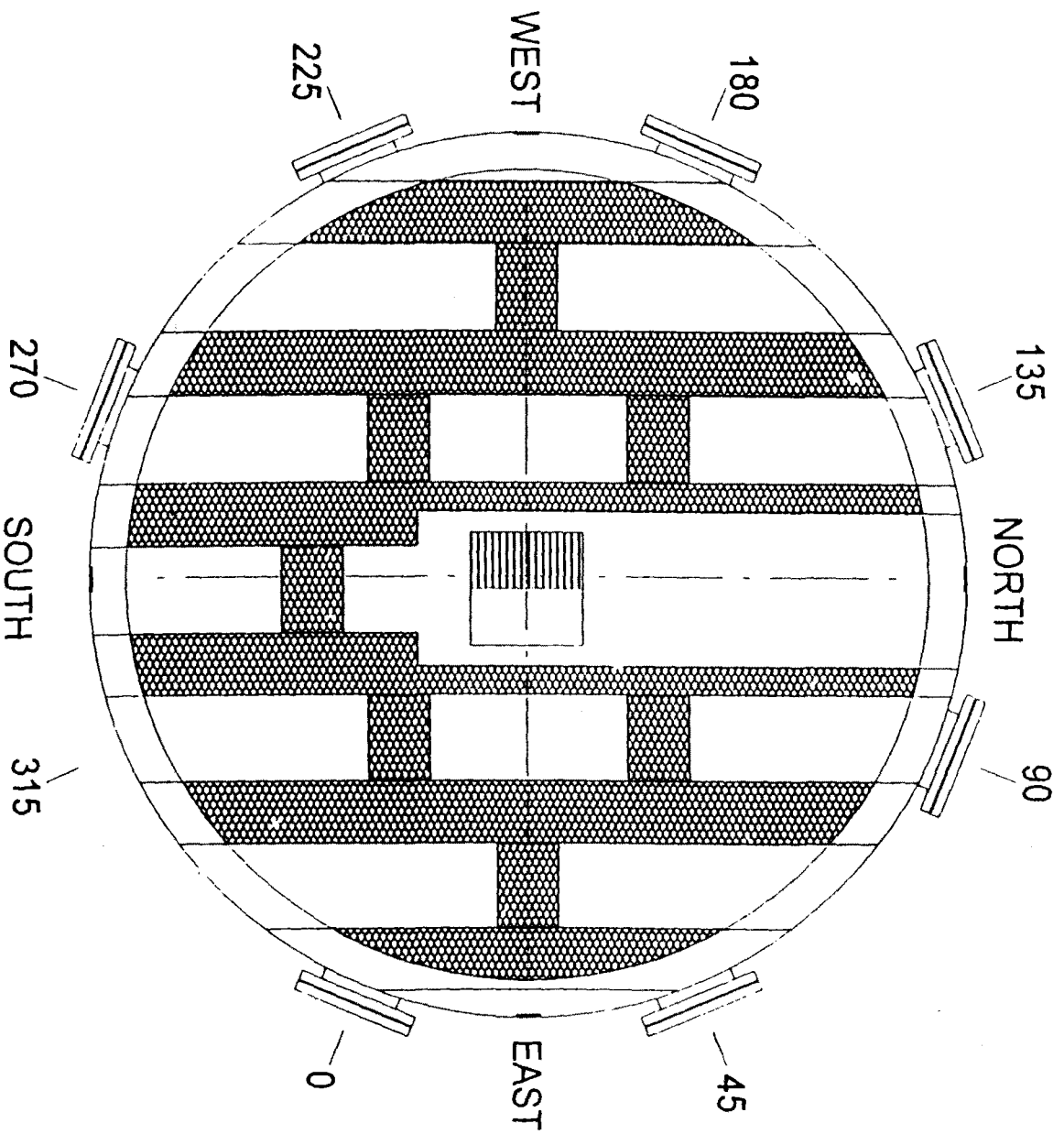


Figure 4. The top view layout of the PAR and the horizontal I-beams.

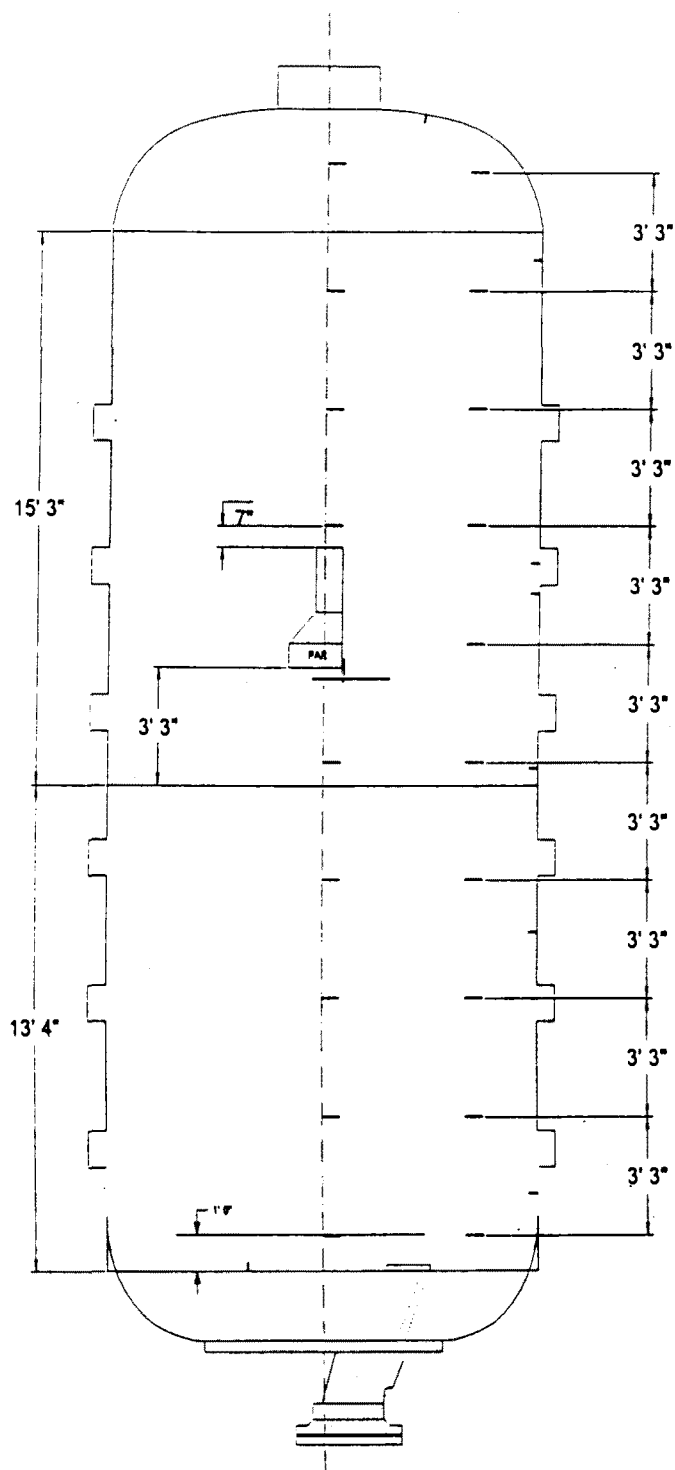


Figure 5 The locations of the thermocouple arrays and the PAR inside the Surtsey vessel.

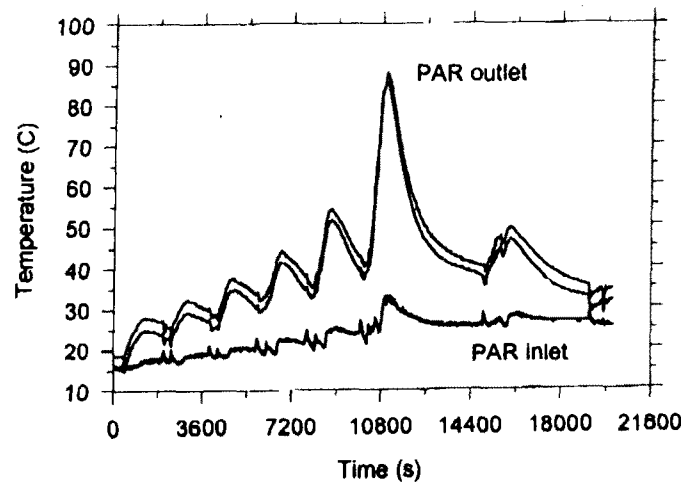


Figure 6. Temperatures at the PAR Inlet and Outlet

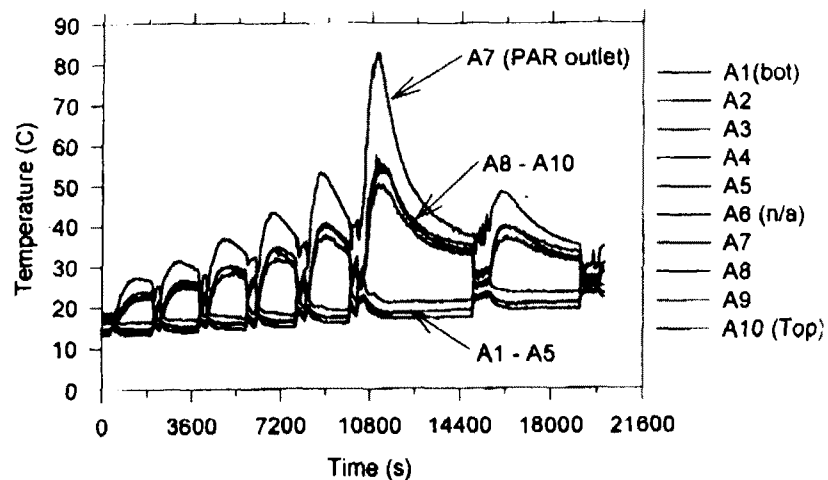


Figure 7. Surtsey Vessel Centerline Gas Temperature.
(Below the PAR A1-A5 and Above the PAR A7-A10)

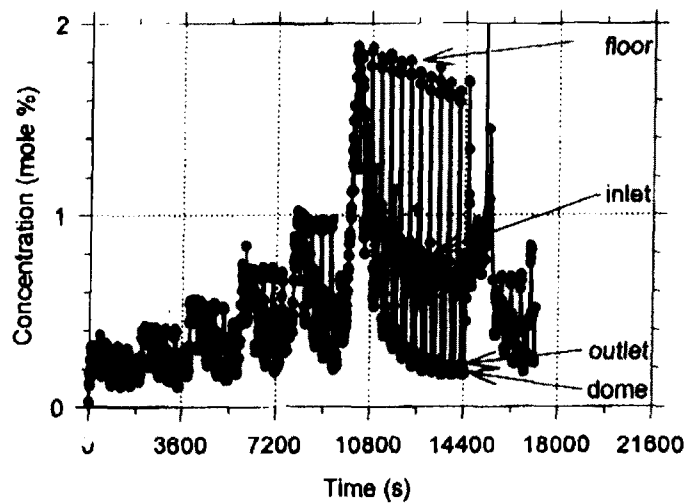


Figure 8. Hydrogen Concentrations

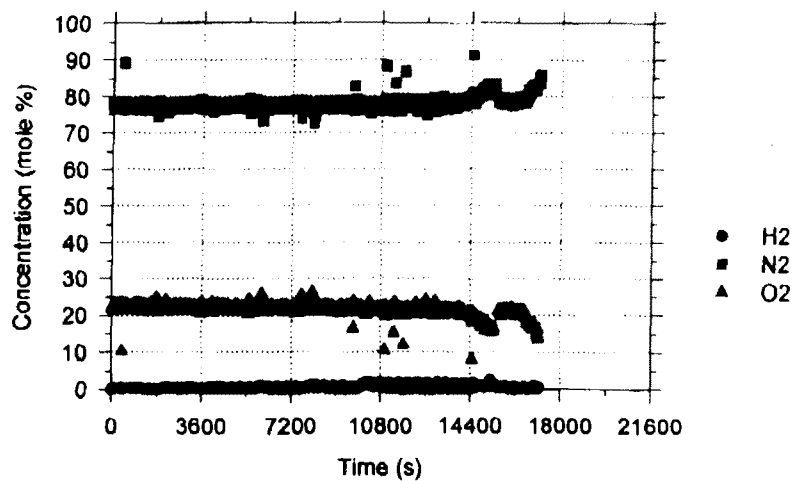


Figure 9. Gas Concentrations

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4.5 H2PAR FACILITY

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1. Introduction

Hydrogen Catalytic Recombiners seem up to now one of the most attractive way to mitigate hydrogen risk during a severe accident in a PWR. Large results of qualification programs confirm that this kind of device is totally passive and works at low hydrogen concentrations and at high steam concentrations. Some tests show that the recombiners keep there efficiency in contact with different types of poisoning like I_2 , ICH_3 , CO, vapours of lubricants... However, some key points are still unknown like the effect of the spray system and the impact of aerosols.

In order to have a better understanding of the behaviour of such devices under severe accident conditions, the H2PAR program is ongoing at IPSN within IPSN/EdF research program. This program is devoted to the catalytic recombiners efficiency in relation with potential poisoning. The effect of the spray system is studied by EDF in the KALI-H2³ facility

2. H2PAR facility

This facility is on the Cadarache Nuclear Center near the RESSAC¹ experiments. The RESSAC team worked during six years on the aerosols transfer in the environment and they made about fifty experiments on it. They are specialized in the production of aerosols with the POLYR² induction furnace. They were, then, adequate to the H2PAR program because the idea of aerosols production in a plastic vessel could be kept.

2.1 Facility design (see figures 1, 2 and 3)

The H2PAR test design is made of a double terphane vessel (about 8 m³). This vessel is fixed on a stainless steel plate with a diameter of 2 meters. A double plastic vessel is needed because the interstitial area is heated by thermal resistance located on the plate to allow thermal insulation of the inner containment.

An induction furnace (Polyr), a catalytic recombiner (Siemens FR 90-150 as in KALI-H2³ experiments) and a measuring device (Whittaker apparatus) are set up inside the vessel.

A gas mixture will be flown in the plastic vessel. This mixture could be constituted by H₂, H₂O, Air and eventually CO and CO₂.

The evolution of different parameters will be followed during an experiment : gas temperature, H₂ concentration, catalytic plates temperature and inlet flow velocity.

Some tests have been made before using the terphane vessel :

- hydrogen permeability
- behaviour in case of incidental deflagration. This facility is able to accept large H₂ deflagration, up to the stoichiometric mixture, in a volume of 8 m³, delimited by a plastic and transparent pocket.

These tests were positive : so this kind of vessel can be used for the H2PAR program ; its advantages are :

- good control of steam saturation (the plastic vessel is able to adjust its total volume to all partial pressures with a constant total pressure of 1 bar)
- video control
- no cleaning and contamination problems (after use, the vessel is automatically rolled inside a waste box)

To simulate a realistic source term, it is necessary to produce vapors and aerosols analogous to there coming from the reactor core. This core heating is represented by the graphite induction furnace Polyr. Its allowed temperature is higher than the UO₂ fusion temperature (~2900°C) and the used heating kinetic is about 100°C/mn. In the preceding program (RESSAC program), 15 elements were introduced in the furnace ; in the present case, 24 elements will be used to simulate the reactor core inventory (see table below).

Elements	Chemical form
U	UO ₂
Fe	Fe
Cr	Cr
Ni	Ni
Zr	Zr
Sn	Sn
Ag	Ag
In	In
Cd	Cd
I	Csl
Cs	Cs ₂ CO ₃
Sr	SrCO ₃
Te	Te
Ru	Ru
Ce	Ce(NO ₃) ₃ 6 H ₂ O
Sb	
Se	
Ba	
Rb	
Mo	
Rh	
Pd	
La	
Br	

These elements will be heated until a temperature of 2900°C will be reached. This temperature will be maintained during 15 minutes to permit the aerosols growth. Actually, the aerosols yield is already known. It was studied in 50 experiments in RESSAC program and also by 6 experiments in pretest H2PAR program.

2.2 Instrumentation

A large instrumentation will permit a complete control of the test sequence and a video camera will record all apparent phenomena on, and around the devices, including incidental deflagration.

Some measurements are classical : 10 to 16 temperatures on different locations of the containment and the devices to be tested, pressures and flowrate on gas supplied ...

The H₂ measurements are made by two chromatographs using four cells of measurement with a complex automatic control of the sequences, avoiding to take into account all the phases which can be realised in blind time. A performant freezer authorizes the trapping of the condensable part of the gas and aerosols mixture up to minus 100 °C.

The very high temperature of the corium is measured using a bi-chromatic pyrometer which controls, by the help of a micro camera, the exact location of the measurement and the good optical adjustment in a hole of the crucible where the temperature is the same than in the corium mixture.

The measurement of the very small (# 30 cc/mn) flowrates due to sampling of the containment atmosphere is made by « Brooks » thermal effect transducers.

All the measurements and gas transfer data are real time controlled and recorded by computers. A schematic representation of the concerned circuit with direct inscription of the measurements on each real location, allows to have a global view of the situation.

2.3 Test matrix

2.3.1 Preliminary tests : aerosols characterization

The aim of the preliminary experiments is to check if the aerosols production is representative of an accident source in the containment. The checked parameters are :

- concentrations and sizes of the aerosols (during heating, maturation and sedimentation phase) ;
- chemical elements yields ;
- chemical forms. In this case, the results could be compared to the HEVA-VERCORS and PHEBUS experiments ;
- comparison with the accident sequences.

6 experiments have been carried out to study these parameters. The source was :

- RESSAC elements with overall mass multiplied by 2 or 5 ;
- H2PAR elements with various masses.

The tests results will be compared to thermodynamic code calculations (GEMINI2⁴) and to aerosols code calculations (AEROSOLS-B2⁵).

A lot of new and original results was gained in this frame and will be presented during the congress.

2.3.2 Full scale experiments

For the time being, these experiments are planed in 1996-1997.

The main thermohydraulic conditions will be as follows :

- Total pressure : 1 bar
- Maximum atmosphere temperature : 80°C
- Saturated steam or dry air
- Injected hydrogen volume fraction : 10 vol% in dry air (same as KALI-H2 experiments)

First matrix : blank test without aerosols

Its target is :

- experimental apparatus qualification (hydrogen leakage, measurements...)
- reference catalytic effect without aerosols

The results will be analysed and compared to KALI-H2 results taking into account the pressure and temperature effects.

Second matrix : aerosols and hydrogen

In severe accident scenarios, it is difficult to separate the hydrogen and the aerosols releases. Further, in the containment, the concentration of each species (aerosols and hydrogen) has a lot of uncertainties (hydrogen distribution, primary circuit retention...). So, parametric studies are planned.

The study of parameters influence will include :

- catalytic plates temperature at the beginning of aerosols emission
- aerosols concentration

A « cold » catalyst will be the consequence of first aerosols emission and then hydrogen injection. A « hot » catalyst will be heated by an hydrogen injection and, afterwards, an aerosols emission will be made and then a second hydrogen injection to measure the catalyst efficiency.

In the both cases, if it is necessary, the aerosols concentration will vary.

One hydrogenmeter test will be associated to one recombiner experiment. The target is :

- to control if it is working in a so complex atmosphere,
- it is able to follow the dynamic of the hydrogen concentration changes (that is to say, the H2 injection and then the H2 recombination).

From 1997 to 1999, a lot of parameters will be tested in the frame of the catalytic dynamic measurements. Other experiments are planned to test cables fires (S, Cl...) and also aerosols coming from core concrete interaction.

2.4 Time schedule

The building will be finished at the end of the 1996 first term.

Preliminary tests are now ongoing for the aerosols characterization and will start at the beginning of the second term for the first matrix.

The first aerosols experiment will be made at the end of the second 96 term. 3 or 4 other experiments are planned in 1996.

3. Conclusions

These results will also permit after analysis to validate calculation tools. (See presentation of J. Vendel on modelling of aerosols deposition in a hydrogen catalytic recombiner) which will be used for safety assessment purposes.

¹ RESSAC : Rehabilitation des Sols et des Surfaces après un Accident

² POLYR : POLLution of the Lysimeters Ressac

³ KALI-H2 : see presentation of S. Guieu EDF/SEPTEN

⁴ GEMINI2 : thermochemical equilibria calculation code - Thermodata

⁵ AEROSOLS-B2 : aerosols behaviour computer code of the ESCADRE code system



OECD Workshop on the Implementation of Hydrogen Mitigation
Techniques
Winnipeg, Manitoba, Canada, 13th-15th May 1996

H2PAR FACILITY

TEST DESIGN

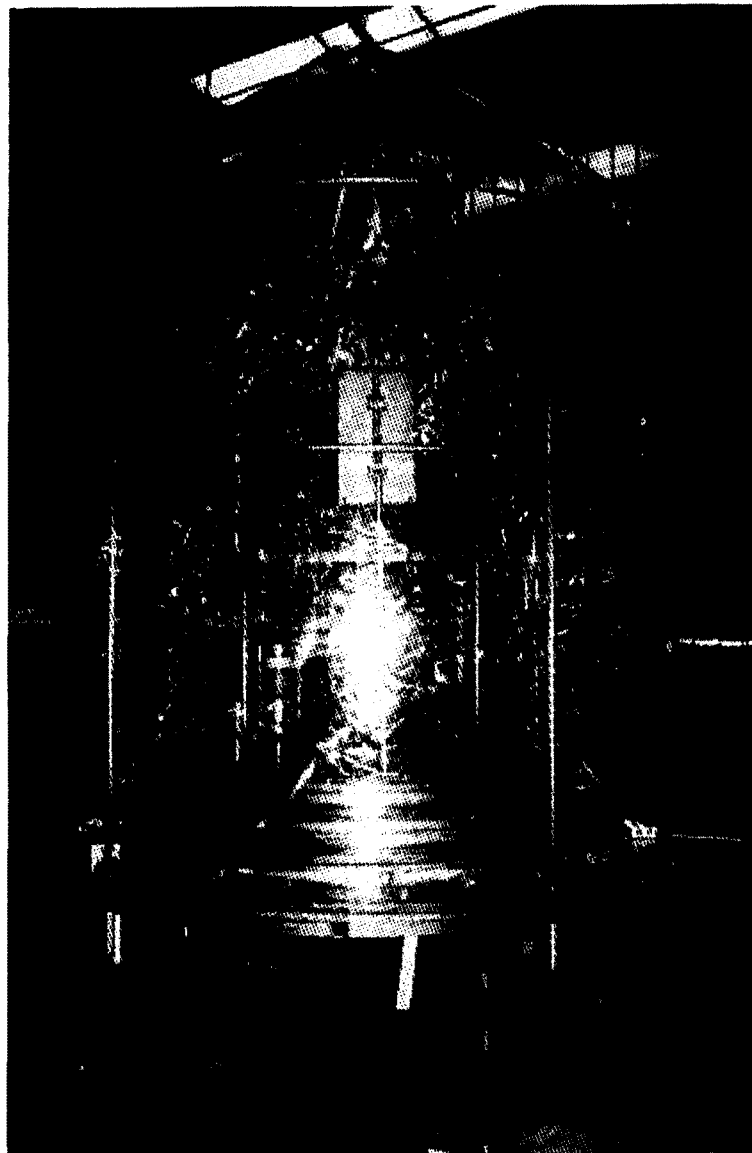


Figure n°1

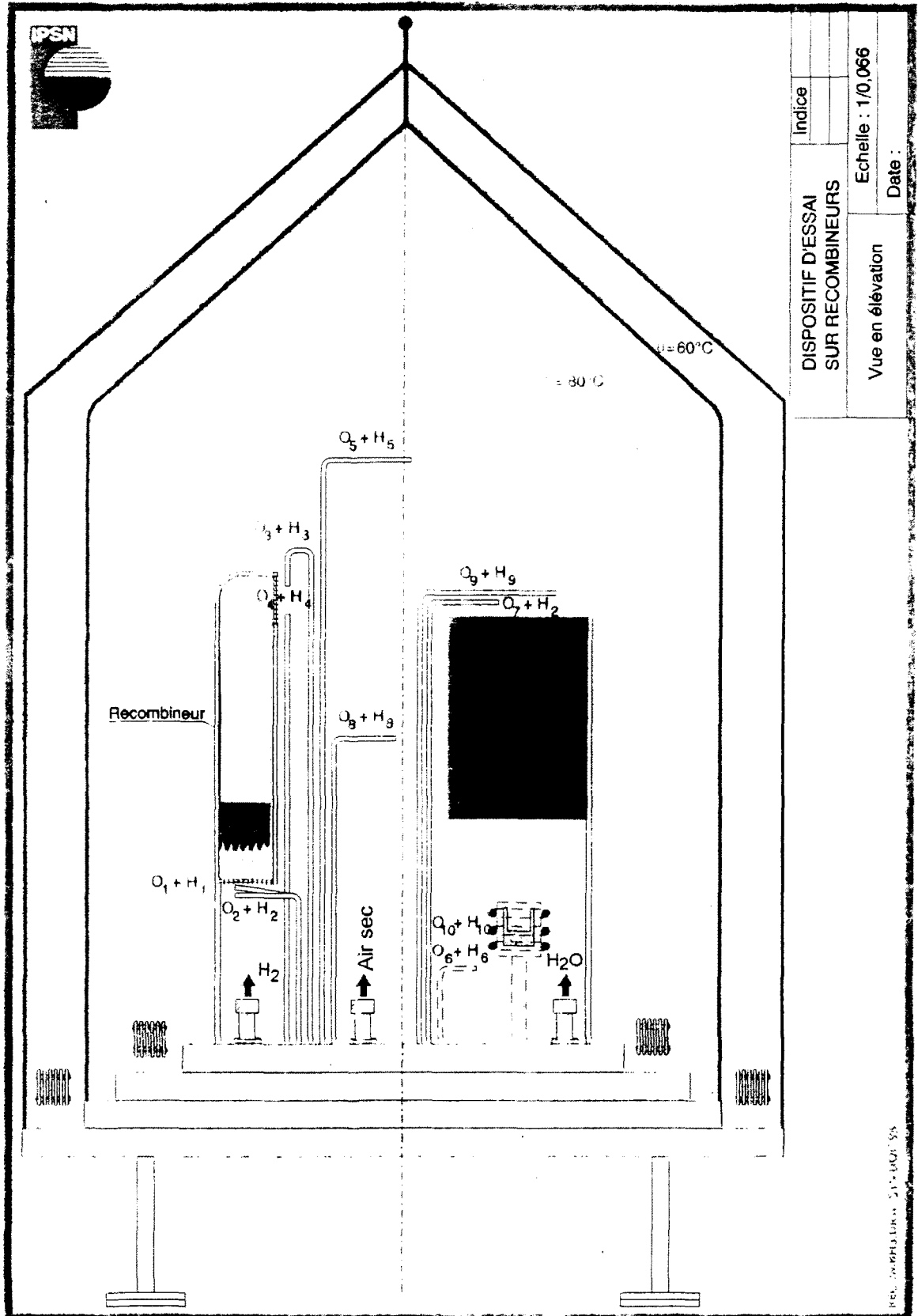
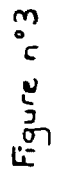


Figure n° 2



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4.6 MODELLING OF THE AEROSOL DEPOSITION IN A HYDROGEN CATALYTIC RECOMBINER

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Abstract : Catalytic recombiners are used to remove the hydrogen released in case of a severe accident in a nuclear power plant, so as to reduce the risk of deflagration or detonation. H₂PAR experiments are carried out to precise the behaviour of recombiners in term of poisoning by aerosols. Firstly, some calculations have been done with the Trio-EF code to assess the structure of convection loops in the experimental tent. We note that when the recombiner is active, it may have a strong influence on the flow inside the tent and may even interact with an other heat source such as a furnace. In the second part, we study the deposition of aerosols on catalytic plates for a given recombiner, when it is active or passive. We list the different mechanisms and quantify them by introducing the deposition velocity. In fact, thermophoresis appears to be the main mechanism, compared to brownian diffusion or diffusiophoresis, which governs aerosols deposition. It favours deposition on «cold» plates and acts against it for «hot» plates.

1/ Introduction :

Catalytic recombiners can be used in nuclear power plants to prevent , in case of a severe accident, the risk of deflagration or detonation with the released hydrogen. They eliminate the hydrogen through the exothermic reaction $H_2 + 1/2 O_2 \rightarrow H_2O$, which occurs on catalytic plates.

The calculations presented here are to support H₂PAR experiments. These experiments will assess the influence of poisoning of catalytic plates on the recombiner efficiency. They will take place inside a tent which contains the tested recombiner and a furnace generating aerosols representative of those emitted if a severe accident occurred.

We consider a given recombiner (Siemens FR 90/150) whose dimensions are about 0.15mx0.15mx1m, with fifteen plates in the low part, and two openings (one under the plates and an upper grid) . We have to distinguish two different situations for this recombiner. When it is surrounded by hydrogen (concentration>2% vol.¹), the combustion takes place and the temperature of the plates is high (several hundreds Celsius degrees). The recombiner will be called «hot» (active). When it doesn't consume any hydrogen, we will call it «cold» (inactive).

¹ Siemens brought to the fore this limit value under which the recombiner is not active

Firstly, we study the structure of the convection loops, including the influence of the recombiner position and the thermal powers released (by the furnace and possibly the recombiner when it is active). From these informations aerosols transport can be assessed inside the tent.

In the second hand, we evaluate the deposition of particles on the catalytic plates, for both situations («hot» or «cold» recombiner). After listing all the mechanisms which act in the deposition process, we assess their orders of magnitude by introducing the deposition velocity. The comparison of these quantities leads to the conclusion of a significative or not significative deposition.

2/ Convection loops inside the H₂PAR tent :

2-1/ Modelling :

The following temperature and velocity fields have been determined using the Trio-EF code, which is a finite-element fluidic code with a multi-dimensional approach.

The real domain is axisymmetric (cylinder with a cone above), but the present calculations are two-dimensional (2D). We consider the plan including the recombiner and the furnace. The correspondence between these 2D calculations and the real 3D case is not obvious. The 2D approach will, indeed, mainly give qualitative results (structure of the convection loops, interaction between the recombiner and the furnace, ...).

The domain is divided in about 2600 meshes, whose mean size is 5cm x 5cm, and 2600 nodes (fig. 1).

Heat losses of the furnace are taken into account by injecting a heat power P_f at the furnace level. Note that the physical presence of the furnace is not modelled. For the recombiner, we have to distinguish both situations. When it is «cold», the external careenage is modelled ($\vec{V} = \vec{0}$ on its surface), and a pressure drop is introduced corresponding to the friction over the plates and through the upper grid. For the «hot» recombiner, we add to the previous modelling the heat power released at the plates level, due to the exothermic combustion of hydrogen. In both cases, the fifteen plates are not physically represented.

The flow entering the «cold» recombiner is due to the external loops inside the tent, and is characterized by the Reynolds number. In the «hot» recombiner, a natural convection flow takes place, governed by the Grashof number. In both cases, the transition regime limit is not reached ($Re_e < 2500$, $Gr_L < 10^9$), and we can thus conclude that the flow regime is laminar.

In the rest of the tent, there is a natural convection flow governed by the furnace. The Grashof number, based on a characteristic length of the domain ($H=3.2$ m), leads to a turbulent regime ($Gr_H \gg 10^9$). So, we use a k-ε model for turbulence description available in Trio-EF outside the recombiner. Some wall functions are introduced at the limit of the domain. Boundary conditions are $\vec{V} = \vec{0}$, $k=\epsilon=0$ on the tent and the recombiner careenage, and $T=80^\circ\text{C}$ on the tent, imposed by the outside. Some fixed value of k and ϵ are set at the furnace level.

Indeed, we fix the furnace power at $P_f=1000\text{W}$ ². For the «cold», then «hot» recombiner, a parametric study is done, for $P_r=100\text{W}$, 500W , 1000W (so $P_r/P_f=0.1$, 0.5 , 1) and $P_r=6000\text{W}$ ($P_r/P_f=6$) that is the order of magnitude of the power released by the reaction for $[H_2]=10\%$ vol. In this way, the study includes different cases, namely when the power of the recombiner is negligible, has the same order of magnitude or is dominant compared with the furnace power.

² Some calculations showed that $0 < P_r < 10000\text{W}$, thus the mean power $P_r=1000\text{W}$

The influence of the disposition of the recombiner has been studied through several positions. The more representative ones are presented in this paper (fig. 2) :

- position 1 : the recombiner is in the higher part of the tent, and out of line with the furnace
- position 1b : same as position 1 with a rotation of 180° so that the upper opening of the recombiner does not face the furnace but the tent close to it
- position 2 : the recombiner is above the furnace
- position 3 : the recombiner is very close to the furnace and at the same level
- position 4 : same as position 2, but the recombiner is not so close

Positions 1, 1b, and 4 are displayed for the «cold» recombiner, and positions 1, 2, and 4 for the «hot» one.

2-2/ Results for the «cold» recombiner :

A natural convection regime takes place in the domain, with an upward flow above the furnace and a downward one near the tent far from the heat source (fig. 3-1). A weak proportion of the air goes inside the recombiner. A region with negligible velocities appears in the middle of the tent. The temperature field is typical of convective exchanges. From the warmest zone (furnace) the rest of the domain is warmed up in the direction of the fluid movement i.e the zone above the heat source, the dome, and, in the end the lower part of the volume, with a certain delay (fig. 3-2).

We note that the most important mass flow rates Q_r through the recombiner are obtained when the recombiner is in a high position (e.g position 1). In fact, one of both opening of the recombiner directly faces the flow coming from the furnace. This flow is just attenuated because of the pressure drop inside the channel. Q_r is almost constant for these upper positions.

For the other positions, the flow rates are smaller. For positions 4 (fig. 3-4) and 1b, the recombiner is, indeed, protected by its upper carenage. For position 3, the velocity field close to the upper opening is strongly upward, with a weak component over the axes of the opening, which does not favour the penetration of air inside the recombiner (fig. 3-3). Note that for this position, the flow generated by the furnace is deviated to the recombiner wall (it could be due to a Coanda effect).

To sum up, the «cold» recombiner has a very weak influence on the velocity field inside the tent, which is governed by the furnace. Moreover, the relative position of the recombiner and the furnace influences in a sensitive way the flow rate Q_r . For the upper position of the recombiner, we obtain the most important flow rate, whereas the smallest one corresponds to position 3. Indeed, Q_r doesn't inevitably grows when the recombiner and the furnace are very close. The flow rate entering the recombiner also depends on the furnace power P_f but we did not deeply study this aspect. Anyway we can remind that the dependance is not so strong because when P_f increases tenfold, Q_r is multiplied by almost three. Because of the simplified approach (2D calculations, the plates are not physically modelled,...), the flow rates calculated just give an order of magnitude. Indeed, for position 1, $Q_r=5.6 \times 10^{-4}$ kg/s (i.e $V=3$ cm/s). Note that this mass flow rate is calculated for the real section of the recombiner openings (15cm×15cm).

2-3/ Results for the «hot» recombiner :

The behaviour of the recombiner strongly depends on the quantity P_r/P_f . For $P_r/P_f=0.1$, an air flow is induced inside it (due to buoyancy force), but this flow doesn't disturb the one generated inside the rest of the tent (fig. 4-1). For $P_r/P_f=0.5$, the influence of the inside flow on the outside is stronger and reaches the region near the openings of the recombiner (fig. 4-2). A convective heat exchange takes place (fig. 5-2). In the last both cases, the convection loop is governed by the furnace.

When $P_r=P_f$, the structure of the flow is different. The velocity field is governed by the presence of two heat sources whose influence are comparable. The flow induced at the furnace level is deviated to the recombiner. This induces a serial flow passing through the recombiner and the furnace (fig. 4-3). An important heat flux is transmitted to the dome (fig. 5-3). Anyway, the velocity field outside the recombiner is variable according to its position (fig. 4-3, 6-1, 6-2).

The mass flow rate Q_r is almost independent of the relative position of the recombiner and the furnace. Only for position 2, Q_r is a bit greater because an «imposed» flow rate coming from the furnace adds to the flow induced by natural convection inside the recombiner. In fact, the main factor which influences Q_r is the ratio P_r/P_f .

For $P_r/P_f=6$, the velocity field is again different because a main convection loop generated by the recombiner takes place from its lower opening to the dome (fig. 6-3). A second loop governed by the furnace provokes convective heat exchanges, but with a noticeably less intensity, which leads to a colder region at the furnace level.

When the recombiner is in a high position in the tent, both loops are completely independent. Nevertheless, the 2D geometry tends to favour this phenomenon, and we may expect that it wouldn't appear to be so marked in the real 3D geometry.

As a conclusion, it appears that compared to the «cold» recombiner, the «hot» recombiner has a more active behaviour because it may influence very much the velocity and temperature fields inside the tent. When $P_r > P_f$, the influence is not so strong but when P_r is of the same order of P_f , the recombiner interacts with the furnace, inducing a main upward flow passing through both objects. Moreover, Q_r is hardly independent of the position of the recombiner. It, thus, just depends on the ratio P_r/P_f . For $P_r > P_f$, the tendency is to obtain independent loops from the furnace and the recombiner, the latter being the most active.

2-4/ Assessment of aerosols transport :

A given quantity of elements is introduced in the pot of the furnace. During the warming phase, these elements are liberated in the air in an aerosol form. Their concentrations will, then, homogenize in the domain because of two mechanisms : the transport by the convection loops and the turbulent diffusion. If the aerosols are injected when the recombiner is «cold», they will mainly be carried in the volume quickly. Because of the velocity (about 20 or 30 cm/s), the homogenizing of aerosols is quite fast.

For the «hot» recombiner, if $P_r/P_f \leq 0.5$, then the flow outside is the same as the one obtained for the «cold» recombiner, thus an identical homogenizing phase.

If $P_r/P_f=1$, since the flow goes from the furnace to the recombiner, we expect that the aerosols reach the catalytic plates quicker than for a «cold» recombiner.

Endly, for $P_r/P_t=6$, the aerosols will tend to get confined in the lower part of the tent, carried by the independent loop of the furnace. Anyway, they will reach the plates by diffusion but this will last longer.

3/ Deposition of aerosols on the catalytic plates :

After listing the different mechanisms for aerosols deposition, we will quantify them by expressing the deposition velocity. Note that mechanisms are treated independently, and they are supposed to be additional (without any coupling phenomenon). Indeed, we don't solve the complete problem of the velocity, temperature and concentration fields between two plates. The range of particle sizes studied is 0.1 to 10 μm .

3-1/ Different mechanisms of deposition and their modelling :

For H_2PAR experiments, we neglect deposition by electrical forces, the sedimentation does not act because of vertical plates, and there is no turbulent diffusion (laminar regime). So, the main mechanisms for deposition are :

- **brownian diffusion** : it induces a movement of particles down the aerosol concentration. In the case of the flow between two plates on which the concentration is zero, brownian diffusion tends to lead to a deposition. It is characterized by the brownian diffusion coefficient :

$$D_b = \frac{k_b T C_c}{6 \pi \mu R_p} \quad (1)$$

If $R_p=0.1 \mu\text{m}$ then $D_b=2.6 \times 10^{-10} \text{ m}^2/\text{s}$ ($T=80^\circ\text{C}$, $P=1 \text{ bar}$)

If $R_p=1 \mu\text{m}$ then $D_b=1.4 \times 10^{-11} \text{ m}^2/\text{s}$ ($T=80^\circ\text{C}$, $P=1 \text{ bar}$)

If $R_p=10 \mu\text{m}$ then $D_b=1.2 \times 10^{-12} \text{ m}^2/\text{s}$ ($T=80^\circ\text{C}$, $P=1 \text{ bar}$)

We use an expression introduced by D.B Ingham (1976, [1]) which gives the deposition rate for a tube with a rectangular section and for a laminar regime. We will apply this result to the flow passing through two plates of the recombiner.

$$\text{Ingham introduced the deposition rate } F_d = \frac{\phi_d}{\phi_e} = \frac{\phi_d}{S_e C_e V_e} \quad (2)$$

He showed that F_d is a function of the only parameter :

$$\Delta = \frac{4 D_b b \rho L}{3 e Q_m} \quad (3)$$

Δ is proportional to the diffusion coefficient, to the length of the tube and inversely proportional to the mass flow rate Q_m .

$$F_d = 2.441 \Delta^{2/3} - 0.300 \Delta - 0.0772 \Delta^{4/3} \quad (4)$$

We can then introduce the deposition velocity :

$$V_{\text{diffusion}} = \frac{\phi_d}{S_d C_e} \quad (5)$$

where ϕ_d is expressed from (2) and (4)

Thus, the final expression :

$$V_{\text{diffusion}} = \frac{(2.441 \Delta^{2/3} - 0.300 \Delta - 0.0772 \Delta^{4/3}) Q_m}{\rho S_d} \quad (6)$$

The study of this relation shows that $V_{\text{diffusion}}$ varies in the same way as Q_m .

- **thermophoresis** : it moves particles down temperature gradient. So it favours deposition on «cold» plates and acts against it for «hot» plates. It is modelled by the deposition velocity :

$$\bar{V}_{\text{th}} = -K \frac{v}{T} \bar{\nabla} T \quad (7)$$

The K factor has been modelled by many authors. N.Montassier made a synthesis of the existing relations (1990, [2]). For our calculations, we consider the one of Talbot, which is supposed to be accurate for the whole range of Knudsen numbers Kn , i.e for each size of particles.

$$K = 2.294 \frac{(\lambda_g/\lambda_p + 2.20 Kn) C_c}{(1 + 3.438 Kn)(1 + 2 \lambda_g/\lambda_p + 4.40 Kn)} \quad (8)$$

The temperature gradient should be expressed by solving the temperature field. To avoid this complete resolution, we approach relation (4) with :

$$V_{\text{th}} = -K \frac{v}{T} \frac{\partial T}{\partial y} \approx -K \frac{v}{T_f} \frac{T_p - T_a}{\delta_t} \quad (9)$$

where y is the axis perpendicular to a plate and directed to it, and δ_t the thickness of the thermal boundary layer

- **diffusiophoresis** : it corresponds to the movement of particles due to the concentration gradients of a (multi-components) gaz. P.J. Whitmore (1981, [3]) gives an approximation of the diffusiophoresis deposition velocity for a multi-component gaz, in presence of a chemical reaction, and for small particles ($Kn \gg 1$).

For the combustion of hydrogen, the particles are surrounded by a four-component gaz (H_2 , O_2 , H_2O , N_2) if we admit that air is mainly composed of oxygen and nitrogen. In this case :

$$V_{diffusioph}^0 = \frac{\sqrt{M_{H_2}} \cdot N_{H_2} + \sqrt{M_{O_2}} \cdot N_{O_2} - \sqrt{M_{H_2O}} \cdot N_{H_2O}}{c \cdot (\gamma_{H_2}^0 \cdot \sqrt{M_{H_2}} + \gamma_{O_2}^0 \cdot \sqrt{M_{O_2}} + \gamma_{N_2}^0 \cdot \sqrt{M_{N_2}})} \quad (10)$$

The exponent 0 indicates that the quantities are estimated at the beginning of the combustion. In fact, we can prove that $V_{diffusioph}$ is not sensibly modified after. Moreover, it is often admitted that diffusiophoresis is weakly dependent of the particles size. So we consider that expression (10) keeps being accurate for bigger particles.

Endly, the diffusiophoresis action depends on the sign of the numerator of relation (10). For the combustion of hydrogen, it is positive, which gives a contribution in favour of the deposition.

3-2/ Aerosol deposition on «cold» plates :

Two main mechanisms have to be considered : brownian diffusion and possibly thermophoresis. Indeed, we have to distinguish two situations. First of all, if there is a thermal equilibrium between the plates and the air, the thermophoresis force is zero. Secondly, when the temperature of the air raises (because of the heating by the furnace), the plates follow the same evolution with a certain delay that we have estimated of several Celsius degrees. For the following study we will consider two cases ($T_a - T_p = 2^\circ C$ and $10^\circ C$). Thermophoresis will thus have a positive action on deposition because $T_p < T_a$.

The greatest diffusion contribution to deposition is obtained using relation (6), in which the mass flow rate is the maximal one calculated thanks to Trio-EF velocity fields i.e $Q_m = Q_r / 15 = 3.7 \times 10^{-5}$ kg/s. The determination of thermophoresis deposition velocity needs to assess first the thickness of the thermal boundary layer. We demonstrate in annexe 1 that the boundary layers over adjacent plates interact from the very beginning of the plates. Indeed, we can't study independently each plate, and we should consider that $\delta_t = e/2$. Moreover, a study that is not developed here shows that at the end of the plates the thermal equilibrium is not reached since a temperature difference remains between the air and the plate : for $T_e - T_p = 2^\circ C$, then $T_a - T_p = 0.5^\circ C$ and for $T_e - T_p = 10^\circ C$, then $T_a - T_p = 3^\circ C$. This element is important because it proves that thermophoresis (according to the axis y) does not disappear along the plate.

We have gathered below the deposition velocity for the diffusion and the thermophoresis processes.

Particule radius	K ($T=80^\circ C$)	$V_{thermo-entrance}(m/s)$ $T_p - T_a = 2^\circ C$	$V_{thermo-end}(m/s)$ $T_p - T_a = 0.5^\circ C$	$V_{thermo-entrance}(m/s)$ $T_p - T_a = 10^\circ C$	$V_{thermo-end}(m/s)$ $T_p - T_a = 3^\circ C$	Δ	$V_{diffusion}(m/s)$
0.1 μm	0.50	1.2×10^{-5}	3.0×10^{-6}	6.0×10^{-5}	1.8×10^{-5}	2.1×10^{-5}	1.5×10^{-6}
1 μm	0.26	6.2×10^{-6}	1.6×10^{-6}	3.1×10^{-5}	9.3×10^{-6}	1.1×10^{-6}	2.2×10^{-7}
10 μm	0.040	9.6×10^{-7}	2.4×10^{-7}	4.8×10^{-6}	1.4×10^{-6}	9.7×10^{-8}	4.2×10^{-8}

Because of the uncertainty on the flow rate Q_m (determined with the 2D calculations), it is interesting to assess the sensitivity of $V_{\text{diffusion}}$ toward Q_m : the dependence is weak since an evolution of a factor 10 in Q_m leads to an evolution of a factor two in $V_{\text{diffusion}}$.

Indeed, for the «cold» recombiner, if a temperature difference exists at the beginning of the plates (even a few degrees), then thermophoresis is the main mechanism for aerosol deposition. Even if its magnitude decrease along the plates, it remains sensibly greater than that of the brownian diffusion process.

3-3/ Aerosols deposition on «hot» plates :

Three mechanisms must be considered in this case : brownian diffusion, diffusiophoresis, and thermophoresis. The latter acts against deposition while the others tend to move particles to the plates. For the «hot» recombiner, the plate temperature T_p may reach several hundreds degrees (about 600°C) according to the concentration of hydrogen. For our study, we fix $T_p=200^\circ\text{C}$ and $T_p=400^\circ\text{C}$.

For the diffusion process, two parameters differ from the previous analysis :

- the brownian diffusion coefficient (proportional to the temperature, cf. (1))
- the flow rate Q_m for a natural convection flow between two interacting plates (cf. [4]):

$$Q_m = \frac{\rho g \beta e^3 b |T - T_e|}{12 \nu} \quad (11)$$

Diffusiophoresis is assessed using relation (10). The molar flux N_{H_2} toward the plates is calculated from the rates given by Siemens (at $P=1$ bar, for $[H_2]=4\%$ vol., $dm_{H_2}/dt=0.03$ g/s and for $[H_2]=8\%$ vol., $dm_{H_2}/dt=0.06$ g/s). Then, $N_{O_2}=1/2.N_{H_2}$ and $N_{H_2O}=N_{H_2}$ from stoichiometric considerations.

Thermophoresis action is determined by (9). As for the «cold» recombiner, we have proven that the thermal equilibrium is not reached at the end of the plates. For a temperature difference T_p-T_e at the beginning, a remaining difference T_p-T_e (of about 70% of (T_e-T_p)) exists at the end.

The deposition velocities for thermophoresis, diffusiophoresis and brownian diffusion are showed below in tabular form.

Particles radius	$T_p=200$ (°C)		$T_p=400$ (°C)		$T_p=200$ (°C)		$T_p=400$ (°C)		$T_p=200$ (°C)	$T_p=400$ (°C)
	$T_e=80$ (°C)	$T_e=120$ (°C)	$T_e=80$ (°C)	$T_e=167$ (°C)	$[H_2]=4\%$	$[H_2]=8\%$	$[H_2]=4\%$	$[H_2]=8\%$		
	V_{thermoph} (m/s)	V_{thermoph} (m/s)	V_{thermoph} (m/s)	V_{thermoph} (m/s)	$V_{\text{diffusioph}}$ (m/s)	$V_{\text{diffusioph}}$ (m/s)	$V_{\text{diffusioph}}$ (m/s)	$V_{\text{diffusioph}}$ (m/s)	$V_{\text{diffusion}}$ (m/s)	$V_{\text{diffusion}}$ (m/s)
0.1 μm	7.8×10^{-4}	5.2×10^{-4}	2.5×10^{-3}	1.9×10^{-3}	5.5×10^{-7}	1.1×10^{-6}	6.8×10^{-7}	1.4×10^{-6}	5.7×10^{-6}	7.4×10^{-6}
1 μm	4.5×10^{-4}	3.0×10^{-4}	1.6×10^{-3}	1.2×10^{-3}					7.5×10^{-7}	9.1×10^{-7}
10 μm	7.1×10^{-5}	4.7×10^{-5}	2.8×10^{-4}	2.2×10^{-4}					1.5×10^{-7}	1.7×10^{-7}

To interpret these results we gather below the extreme values of the deposition velocities for each mechanism :

	$R_p=0.1 \mu\text{m}$	$R_p=1 \mu\text{m}$	$R_p=10 \mu\text{m}$
$V_{\text{thermophoresis}}$ (m/s)	min : 5.2×10^{-4} max : 2.5×10^{-3}	min : 3.0×10^{-4} max : 1.6×10^{-3}	min : 4.7×10^{-5} max : 2.8×10^{-4}
$V_{\text{diffusiophoresis}}$ (m/s)	min : 5.7×10^{-7} max : 1.4×10^{-6}		
$V_{\text{diffusion}}$ (m/s)	min : 5.7×10^{-6} max : 7.4×10^{-6}	min : 7.5×10^{-7} max : 9.1×10^{-7}	min : 1.5×10^{-7} max : 1.7×10^{-7}

Indeed, the magnitudes of brownian diffusion and diffusiophoresis are of the same order. Anyway, thermophoresis is the preponderant mechanism for the «hot» recombiner, for the whole range of aerosol sizes studied ($0.1-10 \mu\text{m}$). It tends to move particles away from the plates, which leads thus to a negligible deposition. This gives birth to a zone near to the plates that is hardly free of particles, hence it is called «dust-free space». This zone is often observed around hot bodies.

In fact, we showed that for a «hot» or «cold» recombiner, the main mechanism acting in the deposition process is thermophoresis. Moreover, W.W. Nazaroff and G.R.Cass (1987, [5]) have studied the magnitude of thermophoresis and brownian diffusion for a vertical plate. For particles of common sizes ($0.1-10 \mu\text{m}$), they found that if $T_p - T_a$ is more than a few degrees, then diffusion is negligible, which confirms our conclusions.

4/ Conclusion :

The recombiner has a different behaviour according to it being «cold» or «hot» (i.e consuming hydrogen). In the first case, the recombiner is passive and the flow rate Q_r passing through it is variable according to its position inside the tent. In the second case, it interacts with the natural convection loop generated by the furnace and Q_r is hardly not influenced by the position ; it is mainly governed by the ratio P_r/P_f .

Moreover, when $P_r > P_f$, the main convection loop generated by the recombiner and the one governed by the furnace tend to be independent. Indeed, the lower part of the tent keeps a colder temperature.

In the second part, we estimate the aerosol deposition on catalytic plates. For a «cold» recombiner, brownian diffusion and possibly thermophoresis act in favour of a deposition. For a «hot» recombiner, diffusiophoresis and brownian diffusion tend to move particles to the plates whereas thermophoresis action is against any deposition. For both cases, we showed that in the considered range of aerosol diameters (0.1 to $10 \mu\text{m}$), thermophoresis is the dominant process which governs deposition. Moreover, for a «hot» recombiner, a hardly free of particles zone appears near the plates («dust-free space») and the deposition is negligible.

Endly, for further studies, one has to pay attention to the following both aspects :

- the laminar regime along the plates is essential because, in case of a transition to turbulence, turbulent diffusion would take place in the deposition process, with a probably predominant magnitude.
- for a «hot» recombiner, one has to check that the surface roughness of the catalytic plates is smaller than the thickness of the «dust-free space» (in general about some tenths of a millimeter), otherwise, deposition could appear to be more significant.

List of symbols:

b	: width of the catalytic plate (=15 cm)
c	: molar concentration of the multi-component gaz (mol/m ³)
C _c	: correction factor of Cunningham
C _e	: particle concentration at the beginning of the plates (kg/m ³)
D _b	: particle brownian diffusion coefficient (m ² /s)
e	: distance between two plates (=1 cm)
F _d	: deposited fraction = ϕ_d/ϕ_e
g	: acceleration of gravity (=9.8 m/s ²)
Gr _x	: Grashof number (calculated with the characteristic length x)
H	: height of the tent (=3.2 m)
k	: kinetic turbulent energy (m ² /s ²)
k _b	: Boltzmann constant (=1.38×10 ⁻²³ J/K)
Kn	: Knudsen number = l_m/R_p
l _m	: mean free path of air molecules (m)
L	: height of catalytic plates (=15 cm)
m _{H2}	: mass of hydrogen
M _i	: molar mass of species i
N _i	: molar flux of species i toward (from) catalytic plates (mol/m ² /s), N _i >0
P	: pression (Pa)
P _f	: heat power released by the furnace (W)
P _r	: heat power released at the plates level (W)
Q _m	: mass flow rate of air between two plates (kg/s)
Q _r	: mass flow rate of air passing through the recombiner (kg/s)
Re _x	: Reynolds number (calculated with the characteristic length x)
R _p	: radius of aerosol particles (m)
S _d	: deposition surface between two plates (≈2Lb)
S _e	: entrance surface of the recombiner (m ²)
T	: temperature (K)
T _a	: temperature of the air (°C ou K)
T _e	: air temperature at the entrance of the recombiner (°C ou K)
T _f	: film mean temperature = (T _a +T _p)/2
T _p	: plate temperature (°C ou K)
T _s	: air temperature at the exit of the recombiner (°C ou K)
V	: air velocity (m/s)
V _e	: air velocity at the entrance of the recombiner (m/s)

Greek letters :

β	: coefficient of thermal expansion (K ⁻¹)
δ _t	: thermal boundary layer thickness (m)
ε	: turbulence dissipation function (m ² /s ³)
φ _e	: flux of C.V (particle concentration × air velocity) through the entrance section of the recombiner (kg/s)
φ _d	: deposition particle flux (kg/s)
γ _i	: molar fraction of species i
λ _g	: thermal conductivity of air (W/m/K)
λ _p	: thermal conductivity of particles (W/m/K)
μ	: dynamic viscosity of air (kg/m/s)
ν	: kinematic viscosity of air (m ² /s)
ρ	: density of air (kg/m ³)

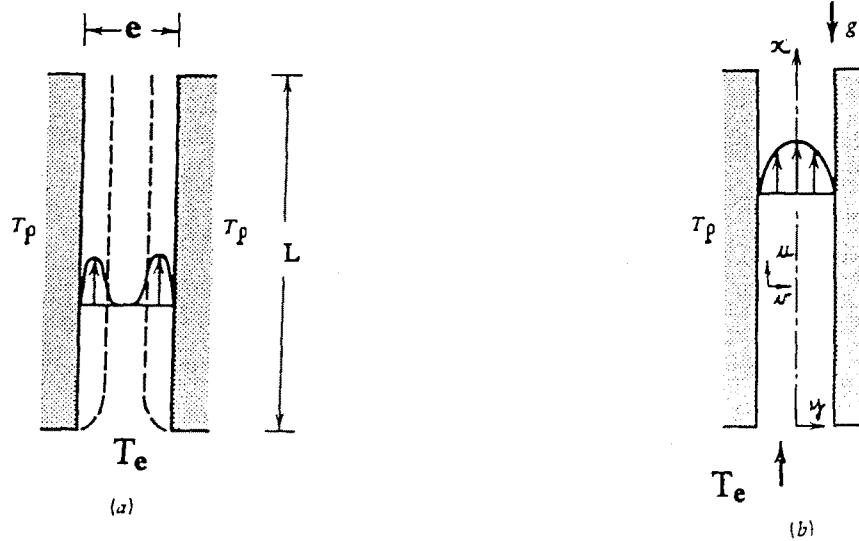
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ANNEXE 1 : thickness of the thermal boundary layer over vertical plates

The present study corresponds to the case of a plate warmer than the air. Thermal boundary layers develop from the bottom of the plate and the air flow is upward. For a «cold» plate, the flow is downward, boundary layers start at the top of the plate, but the following correlations are still accurate .

Let T_p be the temperature of the plates, supposed to be constant. A boundary layer develops along each plate (fig. a). If its thickness reaches the value $\delta_t = e/2$ (where e is the distance between the plates), then the boundary layers of adjacent plates interact (fig. b).



Let introduce the local Nusselt and Rayleigh numbers :

$$Nu_x(x) = \frac{h(x) \cdot x}{\lambda}$$

$$Ra_x = \frac{g\beta |T_p - T_e| x^3}{\nu^2} Pr = Gr_x Pr$$

where h is the convection coefficient

Pr is the Prandtl number $= \nu/a$

β : coefficient of compressibility $= 1/T$ if we suppose the air to be a perfect gaz.

We use the correlation of Mc. Adams that is accurate for laminar flow :

$$Nu_x(x) = \frac{h(x) x}{\lambda} = 0.39 Ra_x^{1/4} \quad (1)$$

At the altitude x , we can express the local thermal flux given to the plate (per unit width of it) :

$$h(x) dx \Delta T = \lambda dx \frac{\partial T}{\partial y} \approx \lambda dx \frac{\Delta T}{\delta_t(x)}$$

Hence the thickness of the thermal boundary layer :

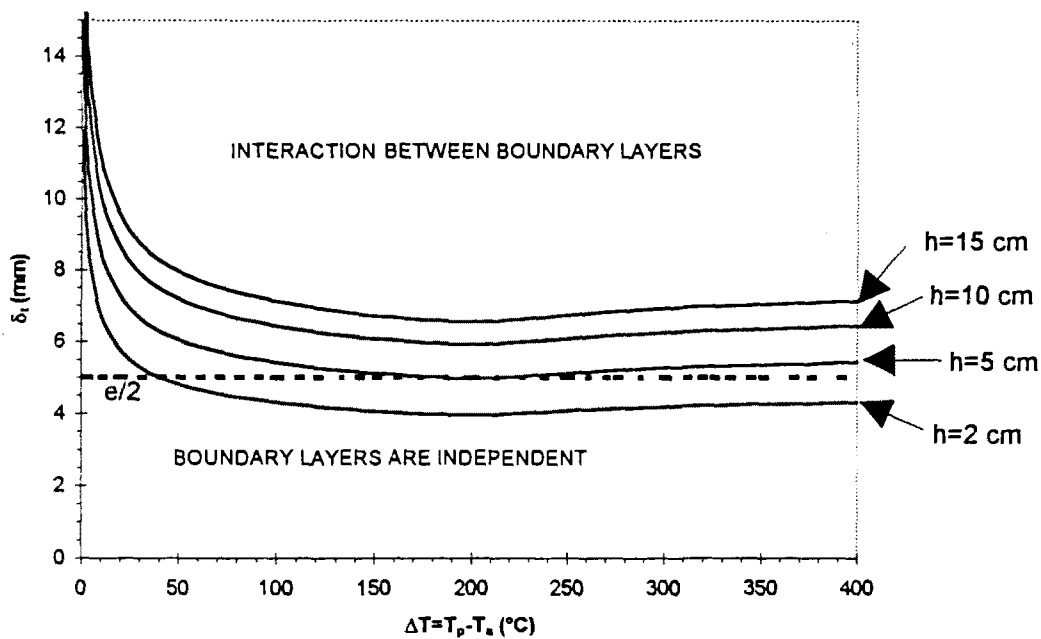
$$\delta_t(x) = \frac{\lambda}{h(x)}$$

Including (1), we obtain :

$$\delta_t(x) = \frac{x}{0.39 Ra_x^{1/4}} \quad (2)$$

Numerical application :

For $T_s=80^\circ\text{C}$, the function $\delta_t = f(T_p - T_s)$ is displayed below.



For a small temperature difference ($T_p - T_s < 40^\circ\text{C}$), the curves show that the boundary layers interact from the very beginning of the plates (at $h=2$ cm). For greater temperature differences, this occurs from $h=5$ cm.

The above curves take into account the evolutions of β and ν with temperature because they are calculated at the mean film temperature $T_f = (T_p + T_s)/2$. These variations are important and they can't be neglected.

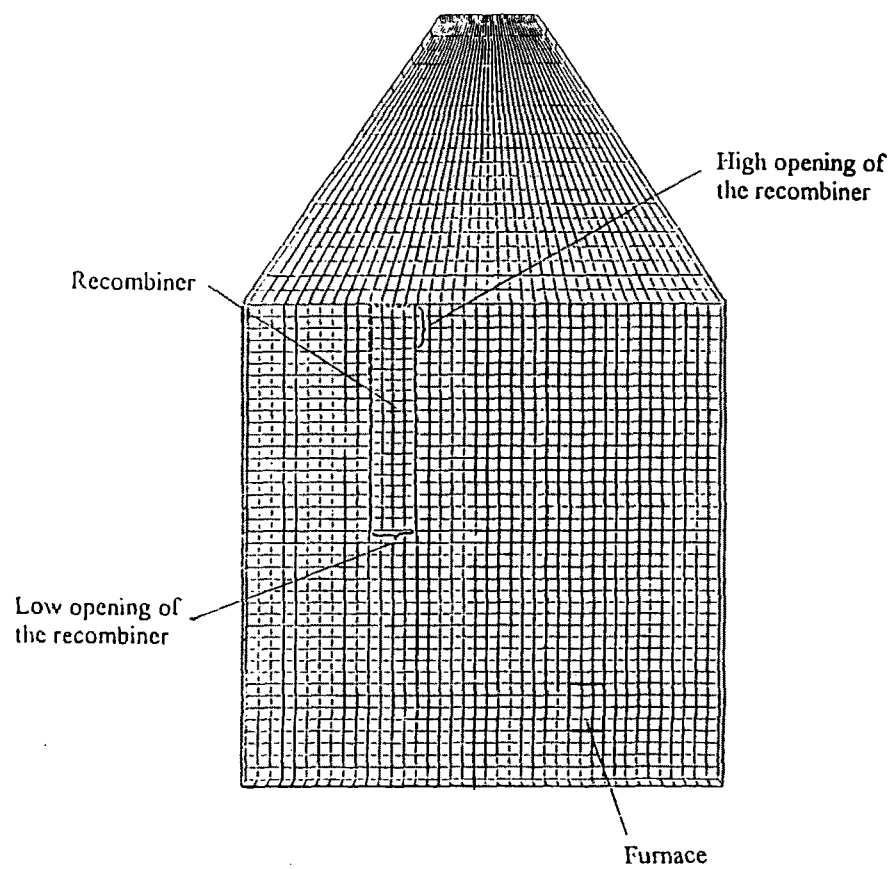


Fig. 1 - 2D meshing of H₂PAR experimental domain

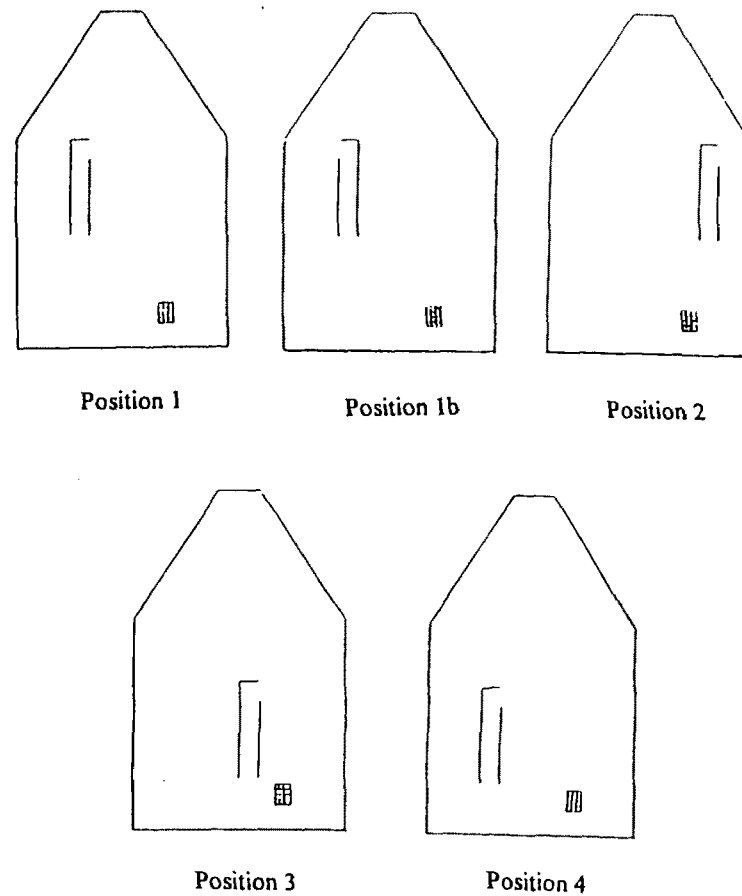


Fig. 2 - Different positions of the recombiner inside the tent

**Fig. 3- Velocity and temperature fields
for the «cold» recombiner**

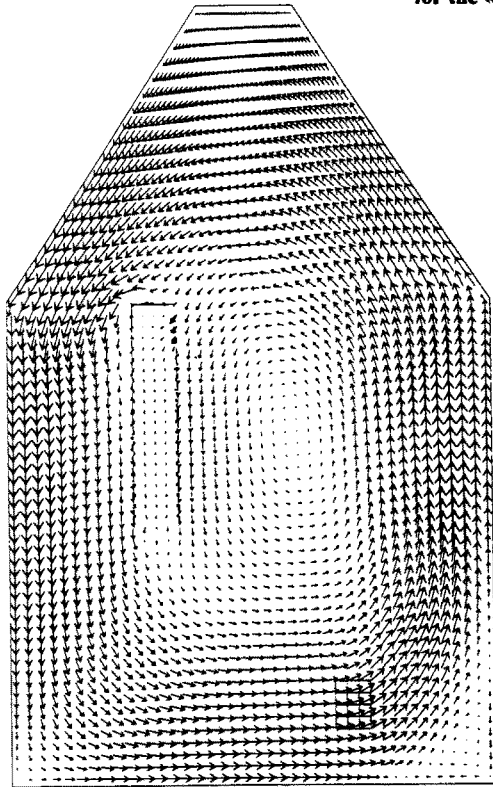


Fig. 3-1 - Velocity field (position 1)

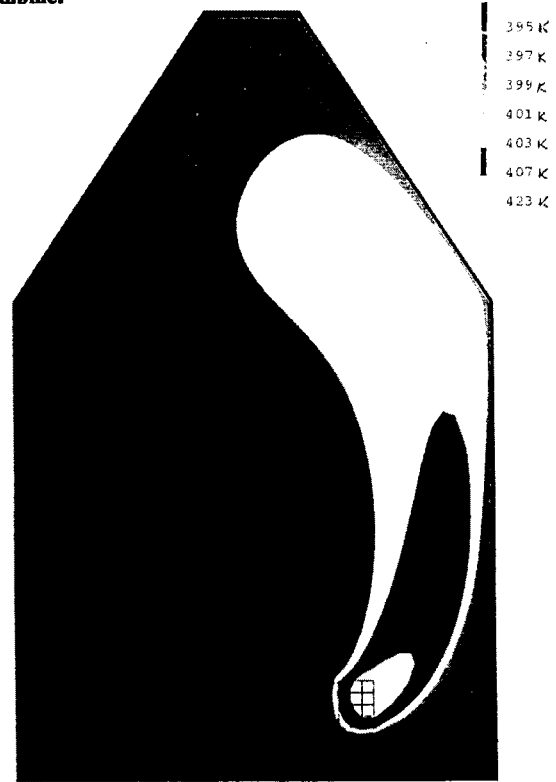


Fig. 3-2 - Temperature field (position 1)

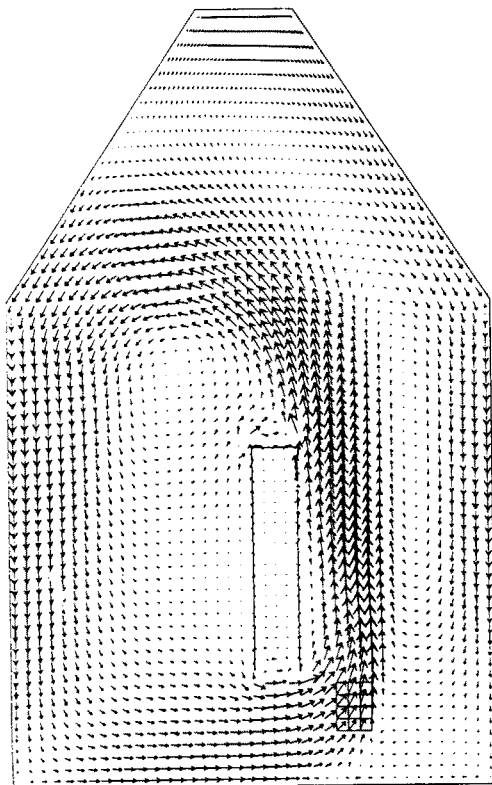


Fig. 3-3 - Velocity field (position 3)

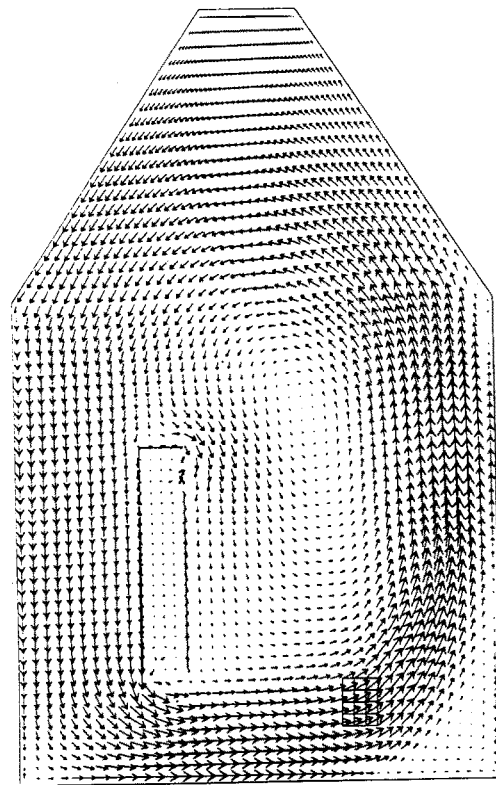


Fig. 3-4 - Velocity field (position 4)

Fig. 4 - Velocity field for the «hot» recombiner
in position 1 ($0 \leq P_r/P_i \leq 1$)

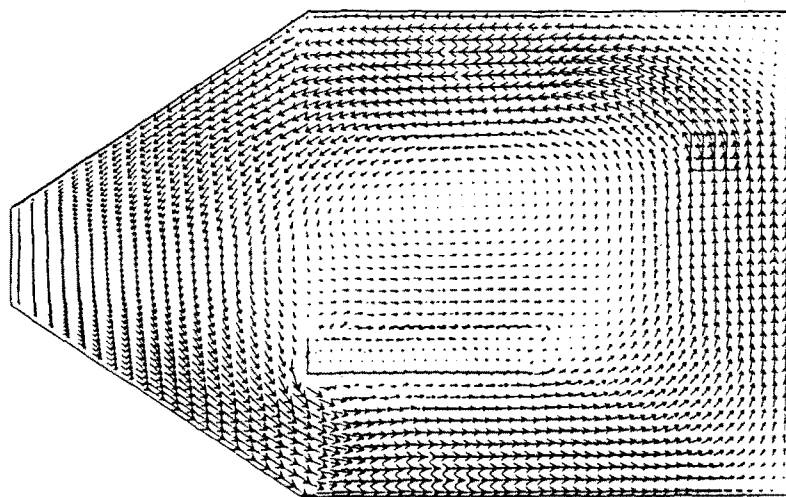


Fig 4-1 - $P_r/P_i=0,1$

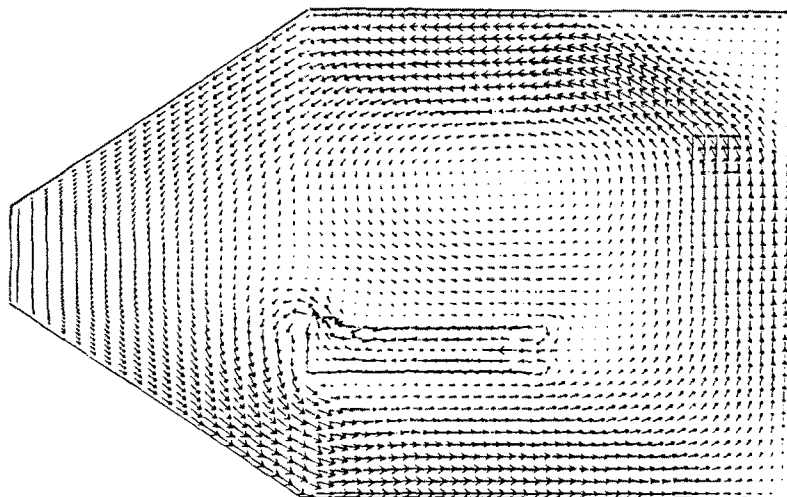


Fig 4-2 - $P_r/P_i=0,5$

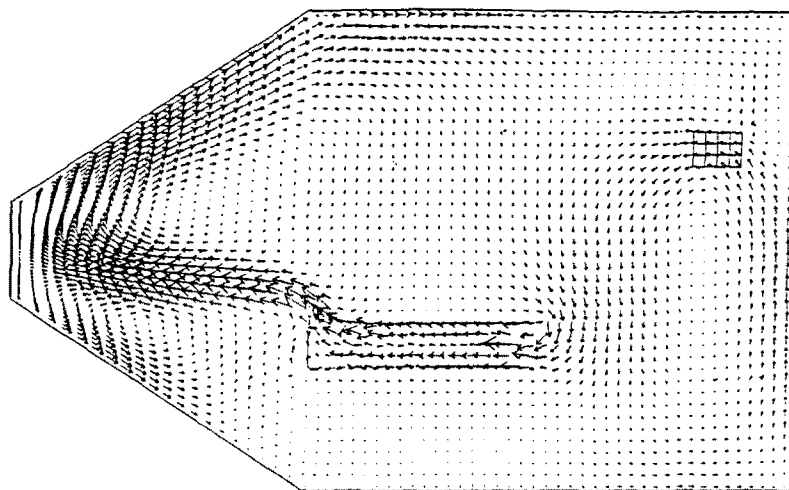


Fig 4-3 - $P_r/P_i=1$

Fig. 5 - Temperature field for the «hot» recombiner
in position 1 ($0 \leq P_r/P_f \leq 1$)

370 K
374 K
378 K
382 K
386 K
388 K
390 K
394 K
398 K
403 K
408 K
412 K

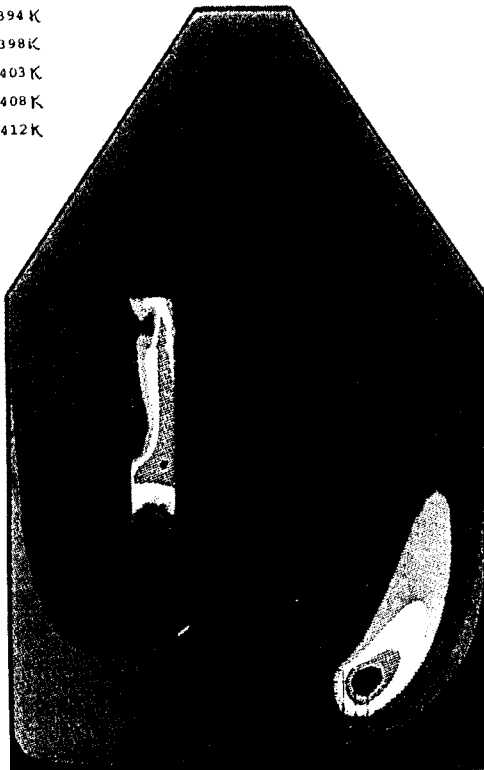


Fig. 5-1 - $P_r/P_f=0,1$

380 K
385 K
389 K
392 K
395 K
400 K
405 K
410 K
416 K
422 K
428 K
432 K

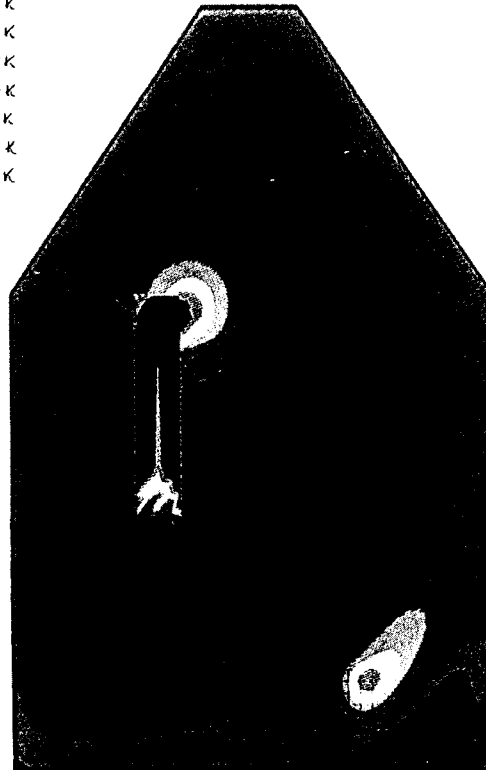


Fig. 5-2 - $P_r/P_f=0,5$

395 K
403 K
410 K
418 K
426 K
430 K
434 K
442 K
450 K
460 K
470 K
478 K

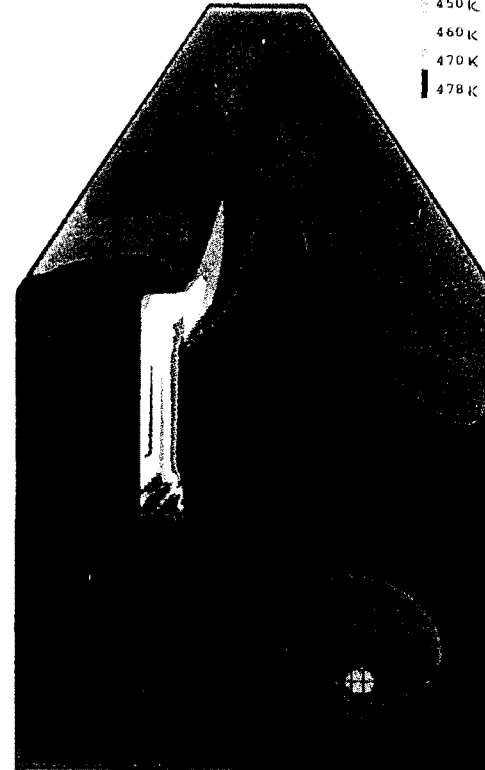


Fig. 5-3 - $P_r/P_f=1$

Fig 6 - Velocity field for the «hot»
recombiner ($P_r/P_i=1$ and 6)

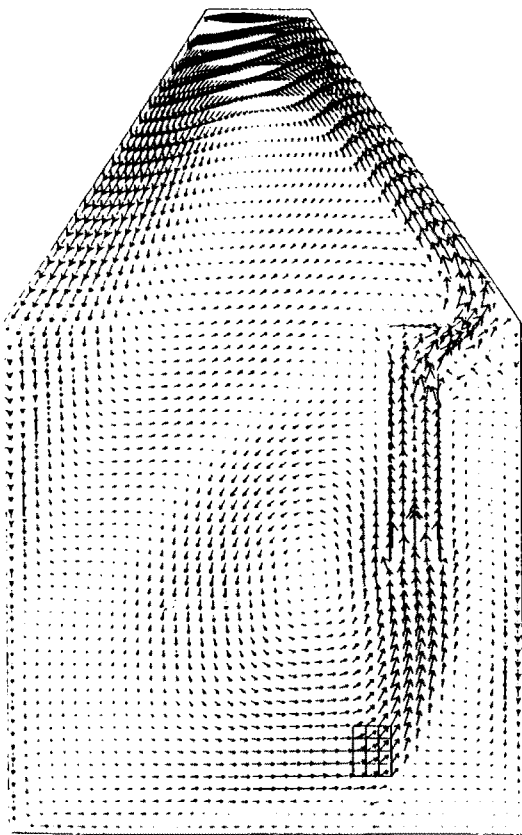


Fig. 6-1 - $P_r/P_i=1$, position 2

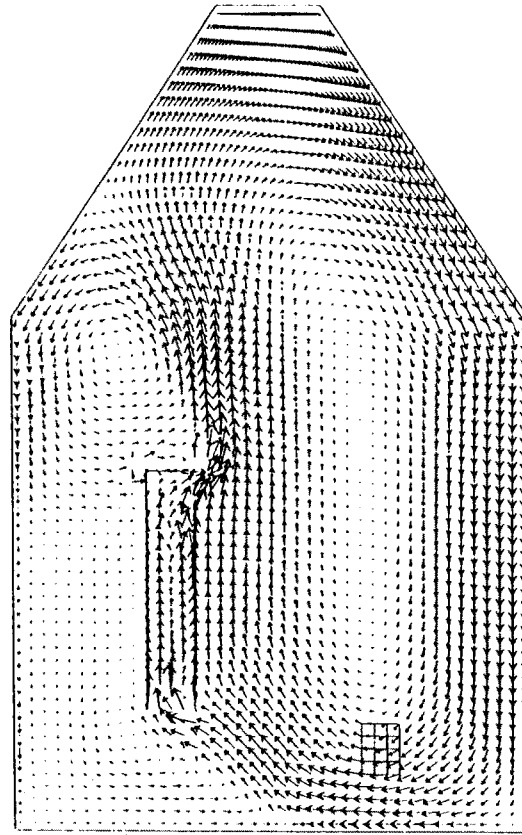


Fig. 6-2 - $P_r/P_i=1$, position 4

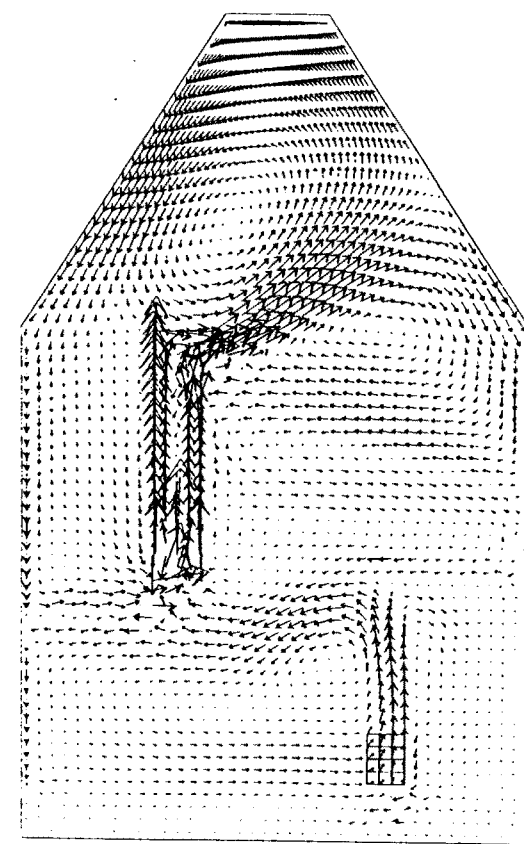


Fig. 6-3 - $P_r/P_i=6$, position 1

4.7 TURBULENT FLAME ACCELERATION AND DETONATION QUENCHING AND REINITIATION—MODELLING AND VALIDATION

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ABSTRACT

For both, the reactor safety in an accidental release of hydrogen into containment compartments and also for the industrial safety of the production, storage and transport of combustibles like hydrogen, propane, methane and others in the Petroleum, Petrochemical and Pharmaceutical Industries, it is of great interest to know how the pressure forces of fast hydrogen combustion processes can be reduced. The numerical study of highly turbulent or detonation driven flame propagation processes is relatively recent because it depends on the availability of high performance computers and specialized numerical algorithms to solve the governing equations of reactive fluid dynamic processes. Numerical simulation can be used at a number of levels to study turbulent combustion and detonations. What is needed is both, to use modelling and numerical simulation to investigate fundamental interactions, and using modelling and numerical simulation as a tool to predict turbulent flame accelerating processes and decoupling or re-initiation of detonation waves in complex geometries of technical applications. Today, modelling and simulation show good agreement with a variety of fast combustion phenomena observed in experiments. Results of reactive computational fluid dynamics codes deliver inputs to reduce experimental parameters and provide the basis for an innovative design of arresters for deflagration and detonation processes.

1. INTRODUCTION

Safety aspects and potential hazards of hydrogen energy systems have to be considered and dealt with in the development of hydrogen as an energy carrier [1].

When used as an energy carrier, hydrogen must be handled safely not only by hydrogen experts, but in addition and even primarily by lay persons. Untrained people must be able to use hydrogen as a fuel with the same degree of safety and with no more involved risks than with conventional gaseous and liquid fuels today. The term risk is defined as a product of the entrance probability of an incident or accident and the possible extent of damage as a result of an occurred incident or accident. In this perspective, risk evaluations are used by safety experts to assess a well balanced and rational safety technology. Two aspects are, therefore, of overriding importance with respect to safety culture in the field of energy technologies. First, prevention of accidental situations, i.e.

reduction of entrance probabilities of incidents and accidents where this is useful and meaningful and secondly, mitigation of postulated accidents, i.e. reduction of damage or consequence of assumed accidents [2]-[4].

2. SAFETY ASPECTS OF HYDROGEN APPLICATION

First of all it is reasonable to compare hydrogen with conventional gaseous and liquid fuels with respect to the basic safety characteristics [5, 6]. Of fundamental importance for all safety related considerations concerning the accidental release of large quantities of hydrogen in nuclear power plants but also in industry in the course of production, storage, transport and utilization of large quantities of combustibles and fuels, is the investigation and assessment of the ignition, turbulent combustion and eventually detonation behavior including the resulting pressure development during accidents [7].

Triggering incidents for most accidental situations with flammable combustible mixtures are fuel leakages caused by material defects, material embrittlement, corrosion, mechanical overload of construction materials, design weaknesses, collision (in the case of vehicles) or insufficient maintenance of equipment.

In the case of an uncontrolled coincidence of released fuel with air or an oxidant and ignition source (the classical fire triangle) one must thoroughly distinguish between combustion processes in confined or even partially confined areas and combustion processes in the open atmosphere.

In the case of an ignition, the possibility of a flame front acceleration to a highly turbulent, fast deflagration and the transition to detonation are decisive for the damage potential of uncontrolled released combustibles [8]-[10]. Experiments with released gaseous and liquid hydrogen in unconfined and completely or partially confined areas show that in unconfined situations, neglecting some very special initial and boundary conditions, no deflagration to detonation transition (DDT) processes have been observed.

However in confined or partially confined compartments there are realistic possibilities that deflagrations in hydrogen air mixtures, with corresponding composition, may lead to detonation due to turbulence promoting structures and interaction of pressure waves with unburnt hot spot areas [11]-[13].

Combustion processes in hydrogen-air mixtures have been subject to a large extent lot of R+D work during the last decade [14]-[28]. Mixtures of hydrogen-air or hydrogen-air-water vapor are formed following the accidental release of hydrogen in systems as well as in nuclear power plants. Once the mixture is formed, it can ignite, deflagrate or under certain circumstances, detonate, figure 1. The initiation, propagation, quenching and re-initiation of detonations, particularly in complex geometries (obstacles, connected compartments etc.) are among the most challenging problems in hydrogen safety [17]. Therefore, both the first step in a DDT-process, which is the turbulent flame acceleration, and secondly the detonative combustion process itself are treated in the next two chapters.

3. TURBULENT COMBUSTION OF HYDROGEN

One of the most important objectives in combustion research studies – either for technical applications or safety aspects – is to obtain better knowledge of the interaction between the exothermic combustion process and the turbulent flow field, particularly in hydrogen-air mixtures with a laminar burning velocity that is ten times as high as, for instance, the burning velocity of propane-air mixtures. We have studied the acceleration process of hydrogen-air flames by a numerical analysis based on the random vortex method and experimental validation. The algorithm models the turbulent eddy movement and provides information on the mechanisms of flame folding from the early phase after ignition to the propagation through different obstacle areas in tubes.

For the calculations a incompressible reactive Computational Fluid Dynamics (CFD) code was developed and experimentally proofed [8, 29].

The flame front tracking is mimicked by discrete volumetric sources due to the exothermic chemistry at the flame front and the laminar burning velocity that makes the flame propagating in its normal direction into the unburnt gases [30].

Figure 2(b) shows the Schlieren photograph of a turbulent flame recorded in a rectangular tube, inner dimensions 100x25 mm, at a distance of 500 mm from the spark ignition point. The gas mixture consists of stoichiometric hydrogen-air. The flame front velocity is in good agreement with the solution of the numerical simulation, figure 2(a) at about 50 m/s.

Obstacle areas are very sensitive to reactive flows [7, 31]. In front of the flame a flow field is built up and develops, particularly in obstacle areas, complex vorticity fields that interact with the arriving flame front. Figure 3(b) gives the impression of a hydrogen-air flame propagation around a single flow obstacle received from the distribution of OH-radicals recorded by means of laser-induced-fluorescence method, from [32]. The flame front, coming from the left, propagates over the obstacle in a certain distance from the top. Interacting with the large-scale vorticity field behind the orifice, the flame front returns with a strongly folded structure. Unburnt gas banks and a lot of small unburnt gas pockets arise. Local extinction due to small-scale vorticity occurs while hot spot areas of unburnt gas lead to sudden ignition [33]. In figure 3(a) the computational results from the developed CFD code displaying the flame front are shown. Using a richer hydrogen-air mixture in the calculations, the flame in front of the obstacle propagates faster due to the higher laminar burning velocity of the mixture.

Different obstacle types cause different vorticity fields. The model of large-scale vorticity is capable of describing the mechanisms. As an example Figures 4(a)-(c) show the simulation results of the flame fronts due to turbulence produced by different obstacles.

4. INITIATION AND ARREST OF DETONATION PROCESSES

As noted above, the possibilities of initiation, quenching and reinitiation of detonations are main concerns in safety aspects related to hydrogen. While the most probable cause for detonation

initiation is flame acceleration in tubes, the most severe damages are to be expected, if these detonation propagate into larger volumes filled with explosive mixture. Therefore, the propagation of a detonation from a tube into a larger volume associated with sudden expansion has been a topic of research since the late 1950s [35]. The critical diameter was proposed to characterize such transmission processes. While a tube diameter is below such a critical diameter, the detonation decays are due to shock diffraction and consequent shock and flame decoupling. If the diameter becomes larger, the detonation recovers from its core and now propagates through the larger volume accompanied by heavily increased destructive power. 1965 Mitrofanov et al. [36] found a correlation between the critical tube diameter and the detonation cell size. For most gas mixtures the critical tube diameter counts about 13 detonation cell width. Since then numerous investigation have been carried out to better understand this transmission phenomena [37]-[40].

However, one important aspect has only recently come to consciousness of researchers: In real geometries there are always walls behind an expansion which can lead to shock reflection thus causing detonation re-initiation even if the tube diameter is below the critical value.

To study this shock/flame decoupling and recoupling phenomena, experimental and numerical investigation have been carried out. For the experiments the Schlieren technique was used. For the numerical simulation inviscid compressible flow was assumed. The chemical reaction was represented by a two step induction/reaction model. The input parameter for this model were determined using a detailed H_2/O_2 reaction mechanism [41]. The fluid dynamics were solved using a MacCormack scheme with a flux corrected transport (FCT) algorithm [42].

In the case of a sudden expansion, experimental and numerical results showed good agreement for varying mixture composition. All phenomena found with physical models could be observed and verified in experiments. These phenomena include sustained detonation propagation (figure 5 left), shock and flame (figure 5 middle and right) decoupling and eventually detonation re-initiation by shock reflection at the confining walls (figure 5 middle) [43]-[45].

The so proofed physical model was used to investigate more complex geometries as for example detonation arresters [46]-[49]. Figure 6 shows the simulation results of a detonation passing a well designed detonation arrester geometry that was optimized on the basis of results gained with reactive CFD calculations.

In the first frame of figure 6 one can see a detonation decoupling due to shock wave defraction. While the main front runs through by-passes, hot jets are permitted to pass through small gaps. The size of these gaps is small enough that no detonation transmission is possible. The hot jet however, causes a deflagrative burn-out of the combustible mixture in the flame arrester area. Finally, when the detonation approaches the expansion area, it meets only burnt gas and decays.

This new detonation quenching principle was then tested in a large scale experiment and its effectiveness was proven.

5. CONCLUSIONS

The impact of combustion processes on the structure of the turbulence in the flow field and the effects of flow field instability on the flame development in tubes containing line or expanded obstacles was discussed. Reactive computational fluid dynamic code calculations of premixed hydrogen-air flame propagation behavior associated with vortex interactions due to a pressure waves crossing flames front have been carried out. Although the physical model is restricted by a number of idealizations, it correctly displays the essential features of large scale eddy flow associated with turbulent flame front effects observed experimentally by means of schlieren technique with the use of stoichiometric hydrogen-air mixture in combustion tubes.

The experimental investigation of unsteady detonation propagation shows that after the decoupling of shock and flame of a detonation front passing a sudden expansion, detonation re-initiation by shock reflections has to be taken into account. The numerical simulation of detonation propagation in complex geometries shows good agreement with the experimental results. Thus it can be used to analyze and improve the possibilities for quenching detonation processes by design of compartments, containments and other technical systems.

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FIGURES

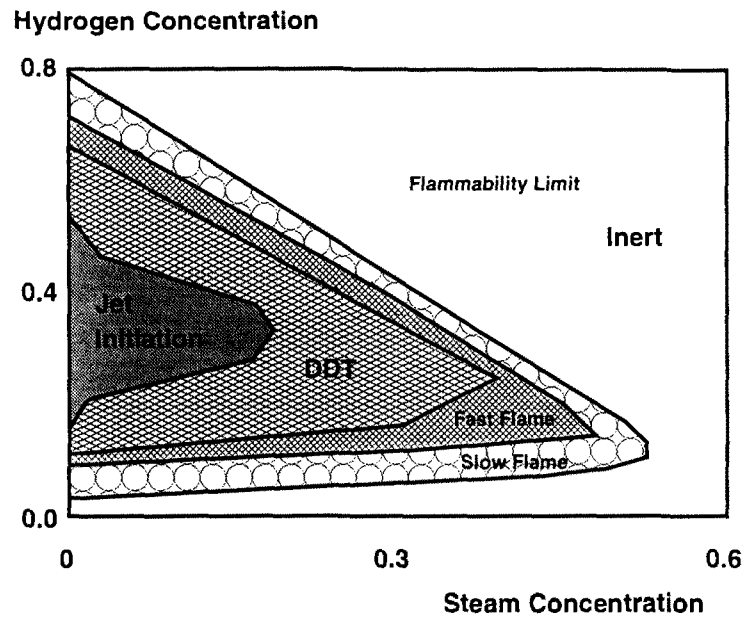
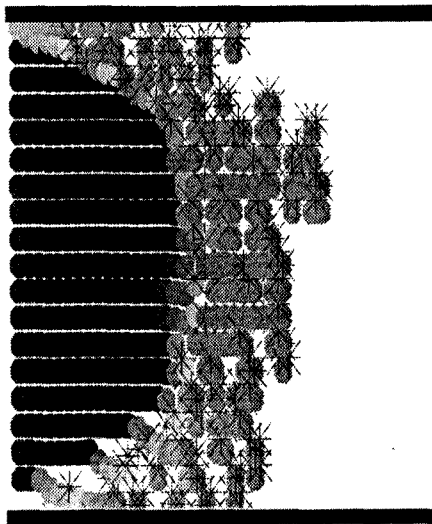
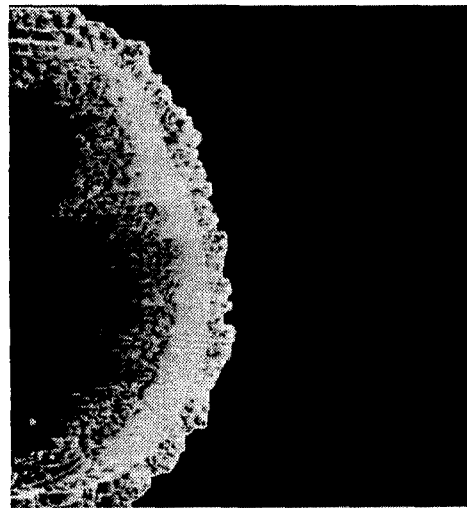


Figure 1: Postulated composition range in which various combustion modes can be realized (schematic).

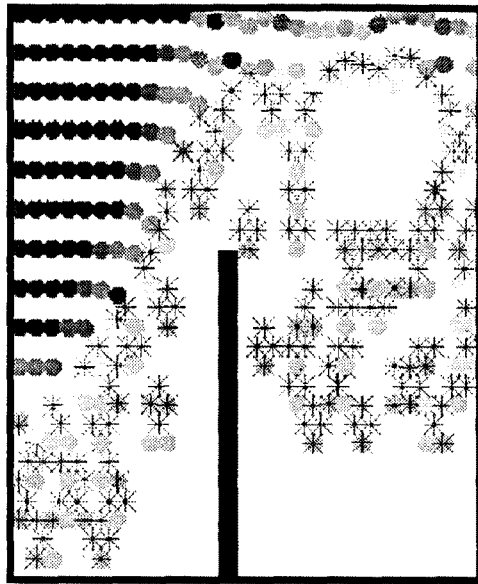


(a) Numerical



(b) Schlieren record

Figure 2: Turbulent flame front.

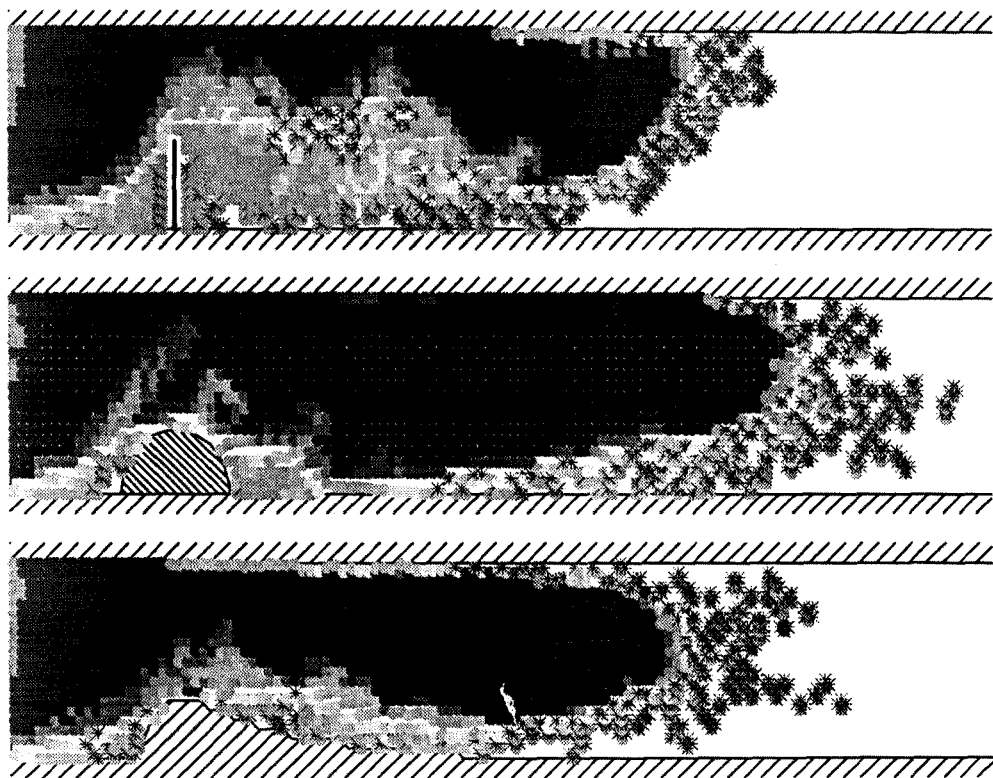


(a) Numerical, DLR



(b) Experimental, Mayinger et al.

Figure 3: Propagation around an obstacle [29, 32].



(a)

(b)

(c)

Figure 4: Flame fronts due to turbulence produced by different obstacles.

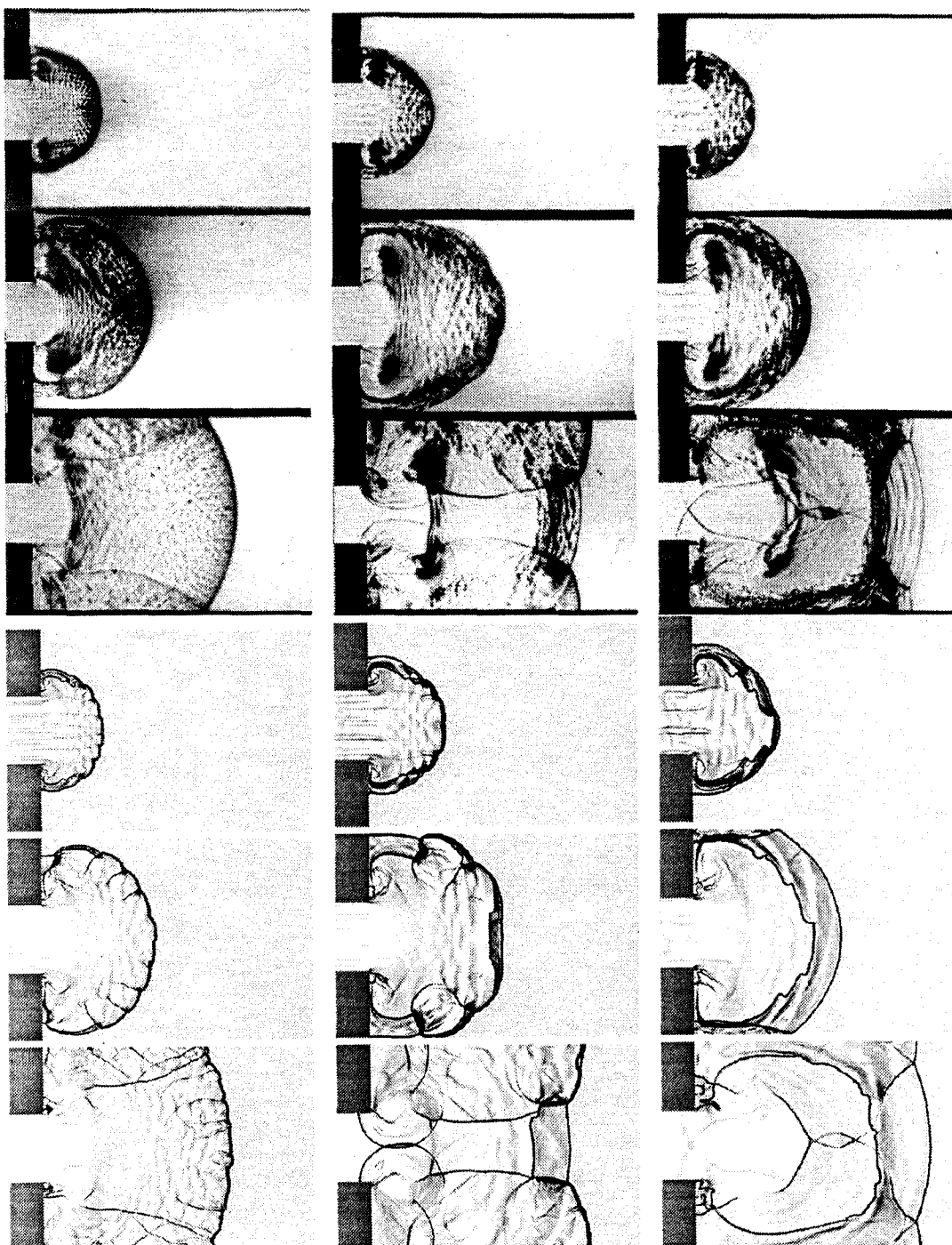
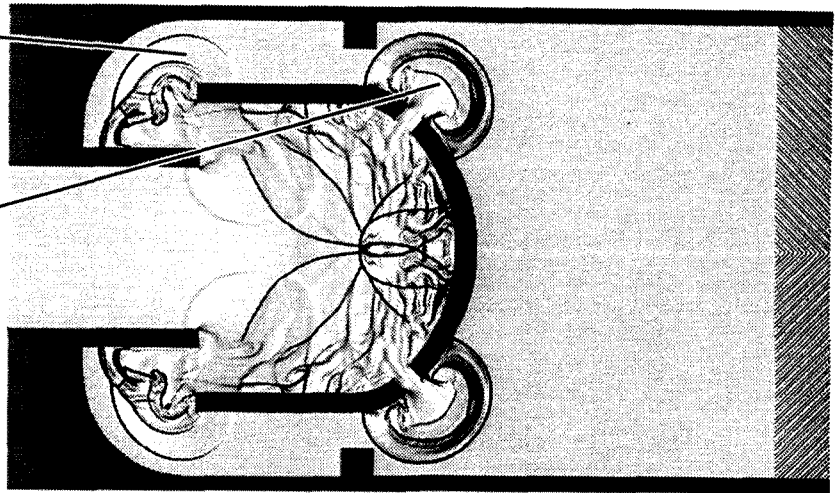


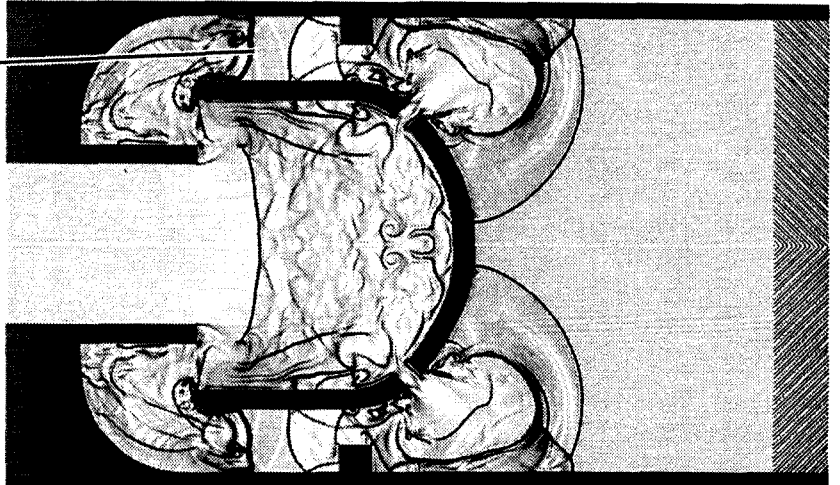
Figure 5: Detonation propagation through sudden expansion (27:80 mm). Top: Schlieren photographs. Bottom: numerical simulation. Left: sustained propagation. Middle: decoupling and re-initiation. Right: final decoupling.

Detonation
decoupling
due to
shock diffraction

Weak
deflagration
caused by
jet ignition



Detonation
re-initiation
due to shock
reflection at
the wall



Decay of
leading shock
from quenched
detonation

Flame Arrester
Area

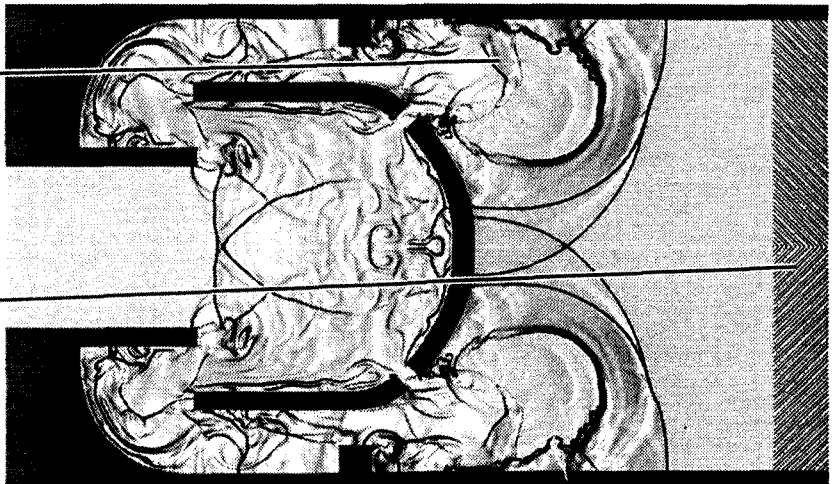


Figure 6: Computational results of a detonation passing a detonation quenching geometry.

SESSION V

POSSIBLE SIDE-EFFECTS AND CONSEQUENCES

Chairman: A. Malliakos (USNRC, USA)

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5.1 TURBULENT COMBUSTION AND DDT EVENTS AS AN UPPER BOUND FOR HYDROGEN MITIGATION TECHNIQUES

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Abstract

A brief review is presented on the limiting conditions for fast combustion regimes (accelerated flames, fast turbulent deflagrations, and DDT events), and on their effect on confining structures. Main attention is given to hydrogen-air-steam mixtures typical for severe accidents in nuclear power plants. Comparison is made of the pressure loads resulting from different combustion regimes. Transient wave processes are shown to be very important for description of the pressure loads. Different limiting conditions are discussed for DDT being the most dangerous combustion event. Possibility of DDT is shown to be limited by the geometrical scale. Detailed description is presented for DDT criterion based on the minimum scale requirement for detonation formation. This criterion gives a conservative estimate that DDT is impossible, if characteristic size of combustible mixture is less than 7 detonation cell widths of the mixture. Conditions limiting possibility of flame acceleration are also discussed.

Introduction

Hydrogen can be generated during loss of coolant accidents in a nuclear power plant. Mixing of hydrogen and steam with air can result in formation of the combustible mixture in the containment building. Average hydrogen concentration in a containment depends on the total amount of hydrogen, and on the volume of the containment building. This average concentration could be rather high - up to stoichiometric one. Different ignition sources can initiate combustion of the mixture. Resulting pressure rise and thermal effect can be dangerous for the containment building and internal equipment. Different mitigation concepts against hydrogen explosions are being discussed and implemented. The most advanced one is deliberate ignition. Main intention is to burn hydrogen early at low concentrations, when benign burn is more probable. Desired results are diffusion flames and/or slow deflagration. However, mixing does not necessarily results in the uniform mixtures. Some parts of the mixture can principally be of high reactivity, which is sufficient to support fast combustion modes. Other mitigation

concepts, like partial inertization and catalytic recombination of hydrogen, are not able to solve the problem for all postulated accident sequences. Combustion of hydrogen-air-steam mixture cannot be excluded, at least for all containment designs. Under certain initial and boundary conditions the accelerated flames, fast turbulent deflagrations, and transition to detonation are principally possible. It is therefore important to understand the impact of these combustion events, and conditions at which they can be expected. Purpose of this paper is to make a short review of the impact of fast combustion modes and of their limiting conditions.

Flame Acceleration and Fast Combustion Modes

One of the most important phenomena responsible for dangerous explosions is flame acceleration. If combustion is generated by a non-energetic source, like a weak electric spark, laminar flame is the initial result of the ignition. Here and in the following discussion we assume that mixtures are within flammability limit. Diffusion flames represent special case, which is beyond the scope of this paper. Propagation speed of the laminar flame with respect to the gas ahead the flame defines by a local burning rate, which is a function of chemical kinetic rate, heat conductivity and energy release in the reaction. Laminar flame speeds for typical hydrogen-air-steam mixtures are about a few meters per second [1]. These low values are much less than the sound speed. Overall pressure rise due to such a combustion is supposed to be quasi-static. Upper bound of pressure increase is limited by thermochemical properties of the mixture, and can be evaluated with equilibrium calculations [2]. Some values of adiabatic isochoric complete combustion pressures P_{AICC} are presented in Table 1.

However, the scenario of laminar flame is hardly possible to expect in practical conditions. The primary reason is that combustion is accompanied by expansion of combustion products. If combustion starts from the closed end of the volume, flame propagation velocity in the laboratory frame (flame speed) is defined by the burning rate and expansion ratio. The flame speed appeared to be several times greater than the burning rate itself. Products expansion generates gas flow and compression waves ahead of the flame. Interaction of the flow with confining structures leads to generation of vorticity and turbulence. Flame propagates through turbulent region with the flow pattern defined by geometrical configuration. This results in increased flame surface and burning rate, which in their turn results in increased expansion, flame speed and so on. Compression waves generated by combustion becomes sharper with increase of the flame speed. Finally a shock wave can be formed ahead of the flame. Reflection of this shock wave can result in mixture autoignition and further increase of the flame surface. This describes the main mechanism responsible for flame acceleration - positive energy feedback between flame produced flow and the flame itself.

Extremely powerful mechanism of flame acceleration is not able to result in flame speeds exceeding some limiting value. When the flame speed with respect to combustion products rise up to the sound speed in the products, the flow appeared to be choked. "Choked" or "sonic" flames give an upper bound for the speed of turbulent flame propagation. This combustion regime was studied in details earlier [3, 4]. Typical values for the speed of

“choked” flames are about 600 - 1000 m/s. Figure 1 shows examples of flame acceleration and quasi-stationary propagation of fast turbulent deflagration.

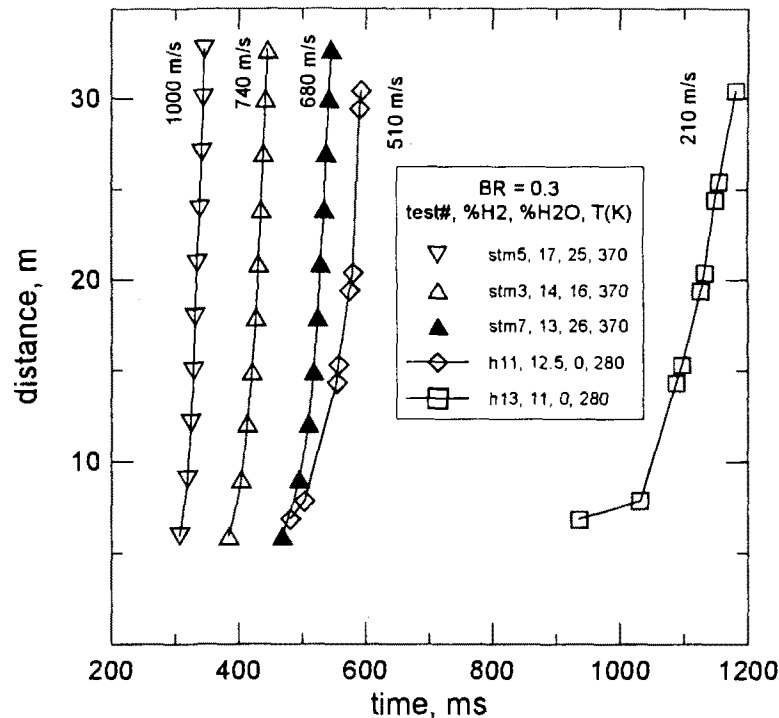


Figure 1. Propagation of turbulent flames in obstructed channel (2.3*2.5 m)

A change of main propagation mechanism is required to increase the propagation speed further. Such a change takes place in the transition from deflagration to detonation. Ignition of compressed mixture behind the leading shock becomes the main mechanism responsible for propagation of the explosion wave. Detonation wave speed depends on the composition and initial conditions. Typical values for severe accident conditions are about 1000 - 2000 m/s. Conditions necessary for Deflagration to Detonation Transition (DDT) will be discussed later.

Impact of Combustion Events

Release of chemical energy in confined conditions results in the increased temperature and pressure of combustion products. The impact of combustion events is thus connected with temperature and pressure loads to the containment and its internal structures.

Thermal loads are due to the heating by the hot product gases. The temperature of the products depends on the mixture composition and initial conditions. Generally it depends also on combustion mode, and on conditions for products expansion. Typical values of adiabatic flame temperatures for several mixtures are presented in Table 1. The values are rather high, in the range of 1000 - 2400 K. The effect of heating depends on the heat transfer rate to confining structures. This process is slow, and transient temperatures, which can be observed in fast combustion waves (sonic flames and detonations), are not able to change the thermal effect significantly. From this point of view, the most

dangerous for neighboring equipment are diffusion flames, because of comparatively long time of the effect.

Pressure loads depend significantly on the combustion mode. If combustion is slow and flame propagates with a speed much less than sound speed, quasi-static pressure rise will be observed. Corresponding overpressures depend on the mixture composition and initial conditions. Maximum pressure rise P_{AICC} can be achieved, if combustion is complete and takes place in closed volume under adiabatic conditions. Table 1 shows P_{AICC} pressures from thermodynamic equilibrium calculations.

If flame speed becomes comparable to the sound speed in the mixture, pressure waves are formed. Their amplitude depends on composition, initial conditions, confinement, and combustion mode. Description of fast combustion regimes requires gasdynamic consideration. Thermodynamic calculations alone are not able to predict transient pressure loads.

Table 1. Equilibrium Combustion Parameters

T, K	H ₂ , % vol.	H ₂ O, % vol.	T _{ad} , K	P _{aicc} , bar	P _{CJ} , bar	D _{CJ} , m/s
300	10	0	1100	4.3	8.1	1290
300	30	0	2390	8.0	15.9	1980
300	50	0	1940	7.1	13.9	2180
300	70	0	1330	5.2	10.0	2310
373	12.75	15	1330	4.1	7.8	1440
373	18.75	25	1720	5.0	9.8	1700

Dynamic Overpressures

Slow Deflagrations

Quasi-static pressure rise represents some global, long-term characteristic of combustion process. In multi-compartment geometry, local overpressures are functions of time, even for slow flames. They defined by competition of pressure rise due combustion, and depressurization due to venting. Figure 2 shows an example of pressure waves generated by deflagration with flame speed less or close to 100 m/s in a large vented enclosure. Overpressures are close to 0.2-0.3 bar. Presence of negative phase is typical feature of vented and unconfined deflagrations. The amplitude of the negative phase can be close to that in the positive phase.

Fast Turbulent Deflagrations

Increase of flame speed results in corresponding increase of explosion wave overpressures. Supersonic combustion wave, with respect to fixed observer, generates compression and shock waves ahead of the flame. The form of the pressure wave depends on the history of flame acceleration, resulting flame speed and geometrical configuration. Pressure waves generated by choked flame are shown in Fig. 3. Peak values are about 6-8

bar, what is about twice less than that from detonation in the similar conditions. Amplitude of pressure wave can be high than value of P_{AICC} , although the duration of this wave is limited. Venting has less significant effect on overpressure generated by fast deflagration compared to the slow one. Even for unconfined fast deflagrations destructive blast waves are observed with overpressures in the range of several bars.

DDT and Detonations

DDT and detonations result in the highest peak values of overpressures. Peak pressure in the stationary Chapman-Jouguet (CJ) detonation wave as well as the propagation speed of this wave are defined by mixture composition and initial conditions. Corresponding parameters P_{CJ} and D_{CJ} are presented in Table 1. Nevertheless this kind of data cannot describe actual peak values, which can be observed in cases of DDT and real detonations [5, 6]. Very high peak values can be achieved in the DDT events, detonation reinitiation, and in reflections [5, 6]. Example of pressure records for DDT is shown in Fig.4. Peak values are about 20 -25 bar, what accedes the CJ value for average mixture composition. This type of pressure loads can be modeled using 3D gasdynamic calculations [6].

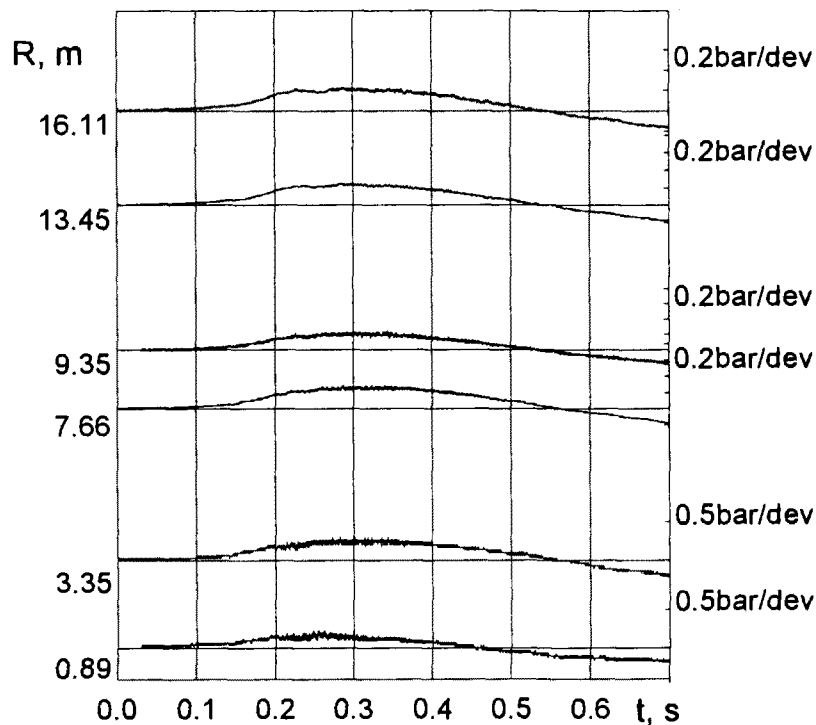


Figure 2. Pressure records for slow deflagration. Non-premixed H_2 - air mixture in vented enclosure of 310 m^3 (RUT facility). Average H_2 concentration 8% vol. Flame speed $< 100 \text{ m/s}$.

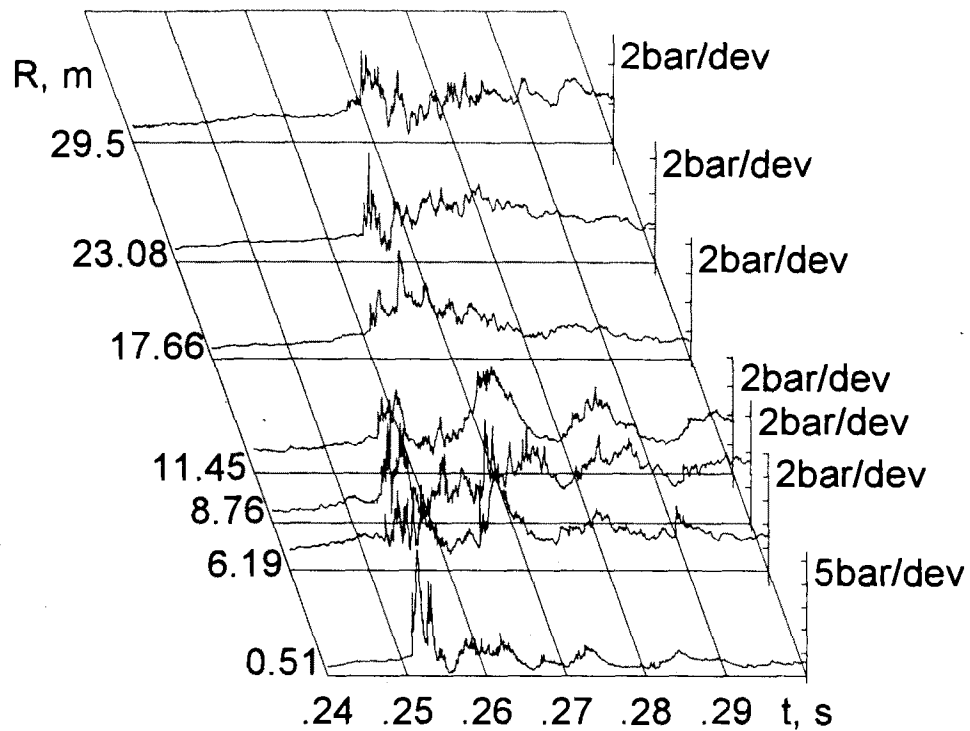


Figure 3. Pressure records for fast turbulent deflagration. Non-premixed H_2 - air mixture (19% average) in RUT facility

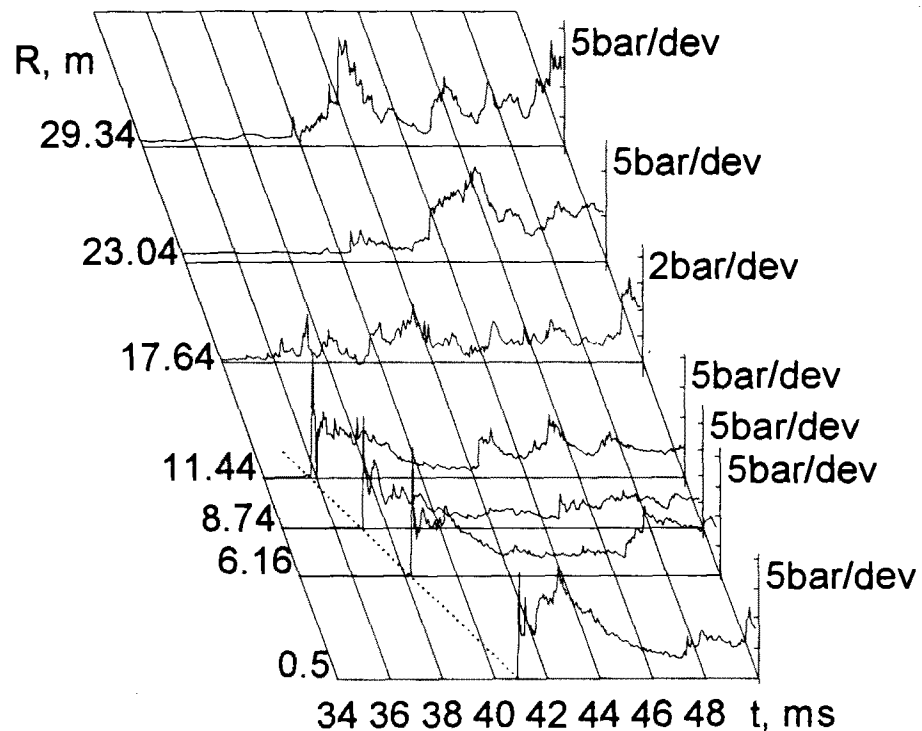


Figure 4. Pressure records for DDT. Non-premixed H_2 -air mixture (42% average) in RUT facility

Impulses

Transient overpressures generated by combustion waves depend on the speed of these waves. The highest values are observed for DDT and detonations. However, the effect of pressure waves depends not only on the peak values, but also on the impulse of the pressure waves. The relative significance of the peak value and impulse depends on the response of the structure, which is exposed to the pressure load. If the response time is less or compared to the duration of pressure effect, peak values are of prime importance. In the opposite case, impulse becomes the defining factor. This is why impulses generated by different explosion mode should be compared as well. Such a comparison is presented in Figs. 5, 6. Figure 5 shows the values of impulse versus time for different combustion modes ranging from rather slow deflagration (< 100 m/s) to fully developed detonations for similar locations of measuring point. Data from confined experiments, and those with small venting area (2 m^2 per volume $> 300 \text{ m}^3$) are collected. Figure 6 shows the same curves normalized with the hydrogen concentration, which represent approximately the energy store in the mixture. The scatter of the data in Fig. 6 does not exceed that of the records of neighboring transducers in one test. One can see that impulse depends mainly on the energy content, and does not on the combustion mode. This specific feature of the pressure effect from different combustion modes should be accounted in the evaluation of structural response.

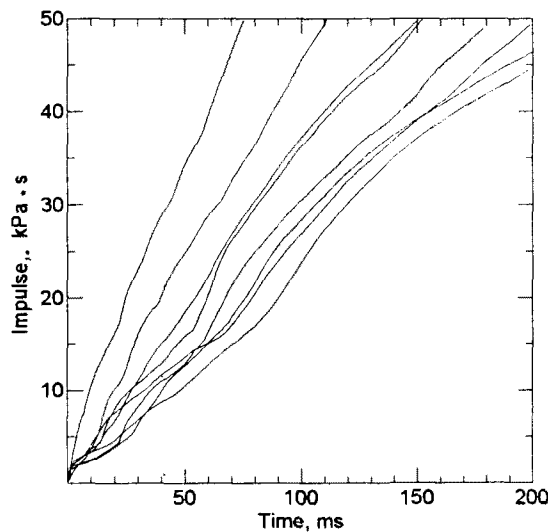


Figure 5.
Impulses vs. time for slow deflagration,
fast deflagration, and detonation

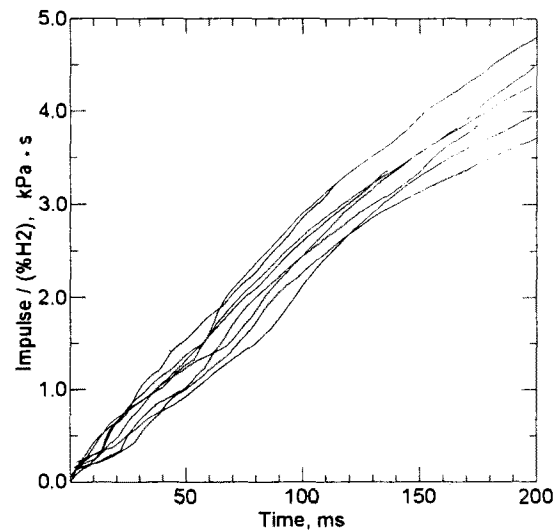


Figure 6.
Impulses normalized with energy content

DDT Phenomenology

Since DDT events and detonations are able to produce the most severe loads to confining structures, the limiting conditions for these combustion events should be addressed first. As it was mentioned above, fast turbulent deflagration precedes usually the DDT event. Deflagrations with a speed about 700 - 1000 m/s generate a shock wave, or several shocks

ahead of the flame. Then, a localized explosion occurs somewhere in the region between the leading shock and turbulent flame, mainly due to reflections of pressure waves and turbulent mixing of combustion products and reactants [7 -9]. The localized explosion can generate a detonation wavelet, which in certain conditions can succeed to propagate through remaining unburned part of the mixture as a detonation wave.

An alternative option is so called "turbulent jet initiation of detonation" [10]. In this case, turbulent mixing of combustion products and reactants is responsible for formation of the localized explosion. We need to note, however, that fast turbulent deflagration in one of the connected compartments is the most probable way of turbulent jet formation.

Following [7, 8], the process of DDT including turbulent jet initiation can be divided into two phases. Phase 1 is creation of conditions for the onset of detonation. This is achieved through flame acceleration, production of vorticity, formation of turbulent jets, and reflections of pressure waves. This phase results in a general situation of formation of an autoignition center somewhere in partially reacted and/or compressed mixture region. The Phase 2 is the actual formation of detonation. It is achieved through localized explosion, amplification of the explosion wave (coupled shock and reaction front), and finally, transmission of the explosion wave from sensitized to unperturbed mixture part. No model is presently available to describe complete set of DDT event. The process itself depends on the complicated interaction of compressible flow, turbulence, and chemical reaction. Many different time and spatial scales are in the fluid motion, turbulence and multi-species chemistry. This makes impossible a numerical simulation of the problem, at least with current computer capabilities. Nevertheless, problem of DDT conditions can be addressed with analysis of different phases and aspects of this phenomenon.

DDT Criteria

Run-up Lengths

Early studies of DDT were focused on determination of the flame propagation distance, which is necessary for transition to detonation. Laboratory experiments in long tubes showed that approximately 20-100 tube diameters are necessary for DDT. Large scale tests in 80th have demonstrated possibility of fast transition to detonation with much smaller run-up length, at about one diameter [11]. In laboratory tests, due to relatively small size of the tubes, large run-up lengths were required to produce conditions for the onset of detonation. It became clear, that run-up length is not correct question to be addressed.

Creation of Conditions for DDT

A number of requirements has been found, which are necessary to provide conditions for DDT (Phase 1 of the process). An example is that the turbulent flame should accelerate to a certain critical value 500 - 1000 m/s to produce conditions for DDT [12]. Alternatively, a high speed jet ignition source (~ 600 m/s) with sufficient size is required [10]. The high speed jet, or fast turbulent flame result in mixing of combustion products and reactants, and are able to generate conditions for localized explosion.

Another scenario is mixture autoignition behind reflected shock waves. Reflections from corners provide the highest local temperatures. It has been found that accelerating flame should be able to generate shock wave with Mach number 2.25 or higher to ignite mixture in reflections, and to generate localized explosion [13].

Some other requirements for generation of conditions for localized explosions can be found. Their application, however, is limited, since no predictive tools are available to describe fast turbulent combustion.

Empirical Criteria for Long Channels (McGill)

Detailed study of DDT in tubes was carried out in McGill university [3, 12]. Flame acceleration and transition to detonation was studied in tubes having 5 - 30 cm internal diameter, and with orifice plates installed inside the tubes. Blockage ratio (BR), fuel type, and mixture composition were variables in these experiments. Detonation cell size λ was used to characterize mixture sensitivity to detonation initiation. This means that different mixtures were compared on the basis of the value of the cell size.

It was found, that $BR = 0.43$ (corresponding ratio of inner and outer diameters of orifice plates is 0.75) provides optimum conditions for DDT. The most effective flame acceleration was observed with this blockage ratio. For this optimum blockage ratio, it was found that the size of unobstructed passage d more than 1λ is necessary for transition to detonation. This criterion can be used as the necessary condition for DDT in obstructed channels. However, it is applicable for very long channels only, having the length more than ten to twenty times the width. It should be noted, also, that empirical character of this criterion cannot ensure its validity far beyond the range of the channel widths (5 - 30 cm) used in the tests.

Long channels with or without obstacles are not typical geometry for the containment. A chain of connected compartments could principally represent a similar geometry.

However, even for long channels many questions remain with applicability of McGill criterion. This is shown in Fig. 7, where data of [14] are used.

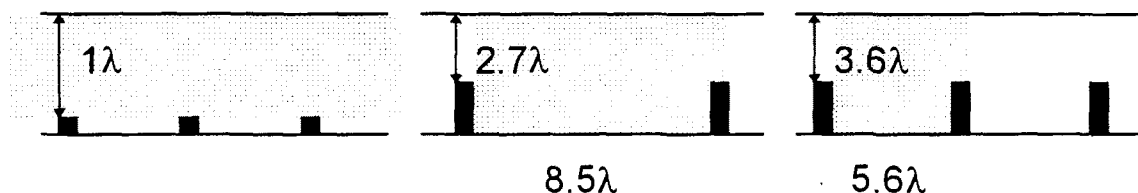


Figure 7. Critical conditions for DDT in long channels with different obstacle configuration

If the blockage is small like it is shown in the left part of Fig. 7, criterion $d > 1\lambda$ is valid. If the blockage is big (central and right part of Fig. 7), much greater characteristic sizes are necessary for DDT. The width of unobstructed passage appeared to be not a defining factor for transition to detonation. It seems that the mixture volume, which can effectively participate in detonation formation, might be of interest. With small blockage, this volume can spread over many obstacles (Fig. 7, left part). In the systems of connected compartments (central and left part of Fig. 7), the volume of the compartment seems an appropriate measure.

Despite of the obvious limitations of the criterion for long channels ($d > \lambda$), it gives an important indication of the effect of geometrical scale on the transition to detonation. The detonation cell size λ increases with changes of the mixture composition below and above stoichiometry. Consequently, the larger the scale the wider is the composition range for DDT.

Minimum Scale Requirement for Detonation Formation

The analysis of DDT conditions at RRC "Kurchatov Institute" is focused on formulation of the minimum scale requirement for detonation formation [15 - 21]. It is assumed that necessary conditions for generation of localized explosion (Phase I) are met, and the process of actual formation of detonation wave is studied. The question addressed is what minimum size of a sensitized mixture should be created in order that the localized explosion can result in a detonation?

Evolution of the localized explosion from an autoignition center includes several stages shown in Fig. 8.

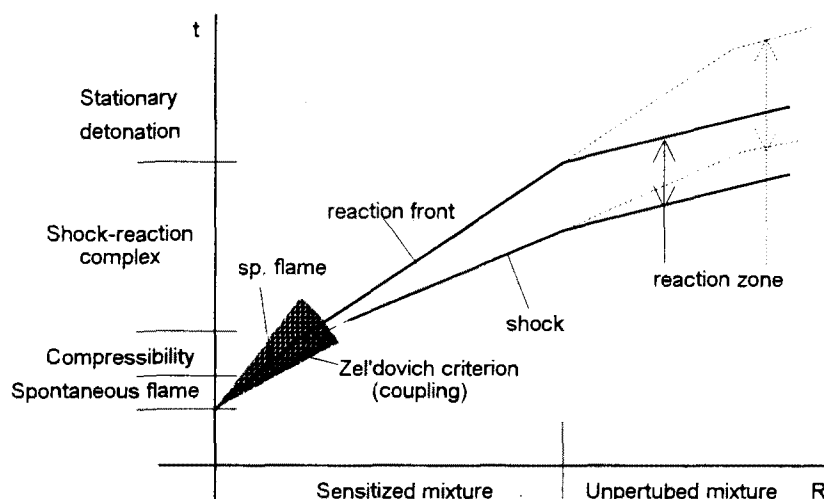


Figure 8. Schematic illustration of wave processes for spontaneous initiation of detonation

Spontaneous flame propagation, in the case of its coupling with gasdynamic processes [22], results in formation of a shock wave - reaction front complex. This explosion wave have to propagate from the sensitized to unperturbed mixture through the region with decreasing reactivity. During this process, the width of reaction zone growth continuously with the decrease of reaction rate. Finally, the width of reaction zone should accommodate that of unperturbed mixture (see Fig. 8). The more is the width of reaction zone in the ambient mixture, the larger size of the sensitized region is required. Continuous increase of reaction zone width results in effective energy losses, because increasing volume of reactants should be heated by the leading shock. If the increase of reaction zone is too fast, the leading shock attenuates, and detonation complex decays. Thus, detonation fails to propagate from sensitized to unperturbed mixture if the gradient in sensitivity is too strong. Certain minimum size of sensitized mixture (detonation kernel) L_m is required.

Figure 9 shows results of calculations of detonation propagation through reactivity gradients for subcritical and supercritical cases [19, 20]. Numerical (1D and 2D) study of the critical conditions for detonation selfinitiation [15 - 19] have shown that the minimum size of sensitized mixture L_m can be expressed as $L_m \approx 7\lambda$, in terms of detonation cell width. This minimum requirement was found for the optimum shape of nonuniformity, and plane geometry. Numerical modeling of detonation transmission gives minimum value of $L_m \approx 7\lambda$ for different effective activation energies of chemical reaction [16, 19, 20]. Analytical model of detonation transmission, which was developed for linear increase of the reaction zone width with distance [20] gives similar values for critical gradient. On this basis the minimum scale requirement for DDT has been formulated, namely, the macroscopic size of sensitized mixture should be not less than 7λ for detonation formation. As a conservative estimate, this means that DDT can be expected only, if characteristic size of the compartment, or combustible cloud is greater than 7λ .

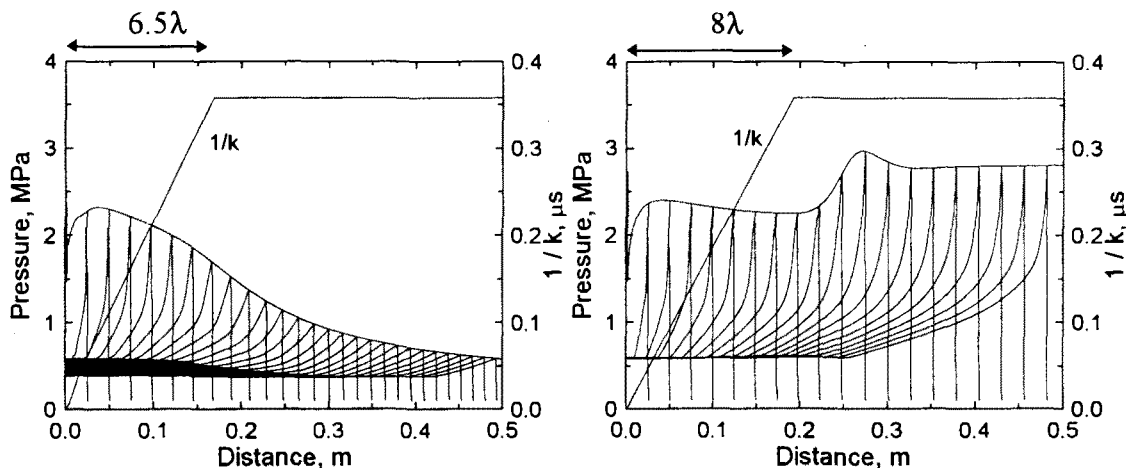


Figure 9. Subcritical case (left) and supercritical case (right) for detonation propagation through decreasing reactivity.

The minimum scale requirement gives necessary, but not sufficient condition. If it is satisfied, it does not mean that detonation should be certainly initiated. Other requirements concerning earlier stages of the DDT process should be met as well. However, if the minimum scale requirement is not satisfied, detonation should not be expected. This important point gives simple conservative estimate for accident analysis.

In the criterion described above, detonation cell size is used as a measure of mixture sensitivity. This allows scaling of DDT conditions found for different mixtures and compositions. Nevertheless, the cell size cannot be considered as a fundamental mixture property. Its application as a scaling parameter should be validated experimentally.

Experimental Data on Limiting Conditions for DDT

A considerable data base has been accumulated in literature on limiting conditions for DDT. This includes McGill small scale tests on DDT and turbulent jet initiation [10, 23], experiments in the FLAME facility [24 -26], Battelle tests with hydrogen-air [27] and

hydrogen-air-steam mixtures [28]. Large scale experiments have been made on DDT and turbulent jet initiation [11, 29, 30].

Recently, large scale DDT experiments with hydrogen-air mixtures were conducted at RRC "Kurchatov Institute". Turbulent jet initiation of detonation was studied in the KOPER facility under semiconfined [16, 18] and unconfined conditions. DDT in large confined volume of 480 m³ was studied in the RUT facility [21]. Similar tests with hydrogen-air steam mixtures at elevated temperatures are currently in progress. In addition, critical conditions for detonation transmission through reactivity gradients were studied in the DRIVER facility [31].

The experimental studies described above were performed under premixed initial conditions. Severe accidents, however, involve cases of dynamic formation of combustible clouds. Ignition can occur during mixing process, when hydrogen concentration is not uniform. Deliberate ignition of hydrogen-air-steam mixtures can be responsible for this scenario. This is even more important in cases of fast hydrogen release, and of hydrogen ignition near the release location.

Special experimental program in the RUT facility was aimed to study possible consequences of deliberate ignition at dynamic conditions [32]. Hydrogen injection with the rate about 0.1 kg/s, or 1 kg/s was arranged in a large vented enclosure (310 m³). Two different locations of the injector were used. One spark igniter was placed at different distances ($\approx$ 5, 10, 15, 20 m) from the injection point. It operated continuously during injection. Sparking rate was 0.1, or 1 Hz.

In the tests, hydrogen cloud was formed moving to the igniter position. Ignition occurred when the cloud boundary reached the igniter. Processes observed in the tests included the following cases:

- (1) no ignition (H₂ is low at ignition time and igniter location);
- (2) ignition, flame acceleration, DDT, and local detonation;
- (3) ignition and flame propagation with a speed up to 100 m/s.

Explosions observed under dynamic conditions were essentially local phenomena. The amount of hydrogen, which was involved in local detonation, was limited by igniter location. It was found that the possibility of DDT depends only on the average hydrogen concentration in the cloud for the given size of the enclosure. The limit (12.5% vol. H₂) is exactly the same as for premixed DDT experiments in the same facility [21]. Distance to igniter has an indirect effect only on the possibility of DDT. For small distances, dangerous concentrations of hydrogen cannot be accumulated before ignition.

Minimum Scale Requirement and Experimental Results

Results of the experiments described above are collected in Fig. 10. The macroscopic size of sensitized mixture L_m , which can be responsible for detonation initiation, is plotted against the detonation cell width of the mixture. Close points correspond to detonation, open - to deflagration. For turbulent jet initiation experiments L_m was estimated as the jet orifice size. For confined DDT experiments, characteristic (average) size of the compartment was used, where DDT occurred. For RUT dynamic tests, characteristic size of the cloud (cubic root of the cloud volume $V^{1/3}$), and average mixture composition for the cell width were used. Accuracy of the cell size data used is within a factor of 2.

The line in Fig. 10 corresponds to the minimum scale requirement ($L_m > 7\lambda$) for detonation formation. This means that above the line, detonation should not be expected, while below the line both the detonation and deflagration are possible. Within the pointed limits of accuracy, the minimum scale requirement $L_m > 7\lambda$ is in a good agreement with the experimental results over the wide range of volumes and compositions. Good agreement is observed both for premixed and non-premixed initial conditions. The comparison presented shows that criterion based on the minimum scale requirement $L_m > 7\lambda$ can be used as a conservative estimate for accident analysis: if characteristic size ($V^{1/3}$) of compartment or combustible cloud is less than 7λ , DDT is impossible. In long channels, McGill criterion ($d > \lambda$) imposes additional limitation. Average composition can be used to estimate λ for non-premixed initial conditions.

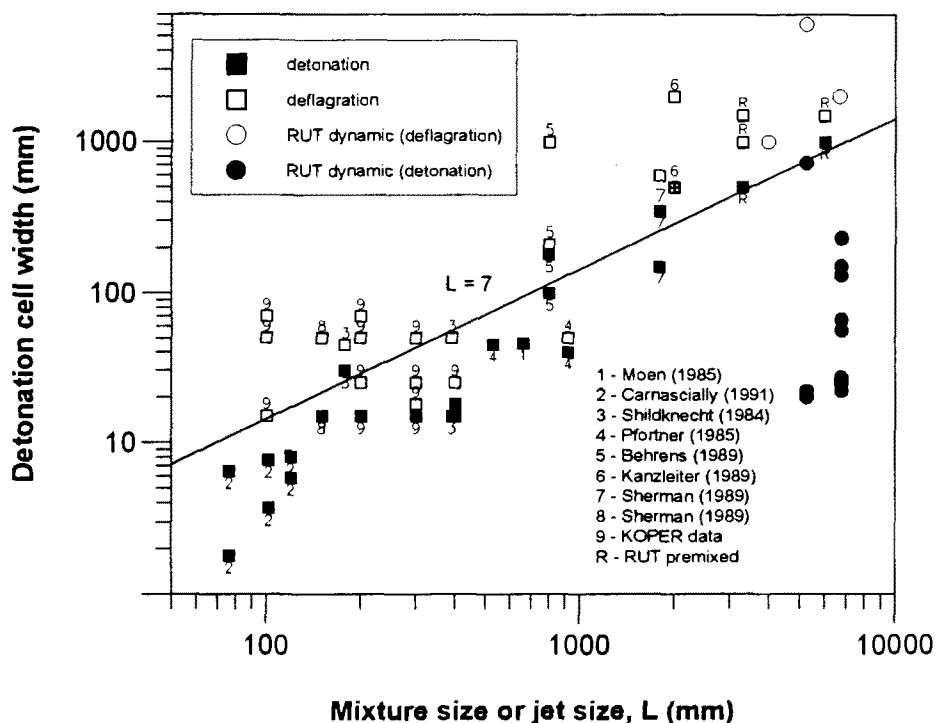


Figure 10. Correlation between detonation cell width and characteristic mixture size necessary for DDT.

Cell size data are necessary for application of the DDT criterion. Detonation cell sizes of hydrogen-air and hydrogen-air-steam mixtures have been measured experimentally [12, 33, 34]. The data are available for different mixture compositions and temperatures in the range 290 -650 K. The measurements were performed for a number of particular compositions and initial conditions. Fitting of experimental data can be used to estimate the cell sizes for arbitrary composition and initial conditions within the range of tested parameters. An example of such a fit is shown in Fig. 11 for $T = 375K$ and atmospheric initial pressure.

The line $\lambda = 5$ m is an extrapolation made with the fitted function. Detonability of such insensitive mixtures is questionable. Nevertheless, this line shows approximately the

overall detonation limit for severe accidents, if the temperature does not exceed 375K. A cloud with characteristic size more than 35 m (!) is required for DDT in the mixtures beyond the line $\lambda = 5$ m. The line $\lambda = 0.1$ m shows another important limit. Mixtures with compositions inside this line are detonable practically in any compartment ($T = 375$), because the limiting geometrical size is about 0.7 m only. DDT possibility for mixtures in between these lines depends on the characteristic size of compartment (cloud), and can be estimated with 7λ criterion.

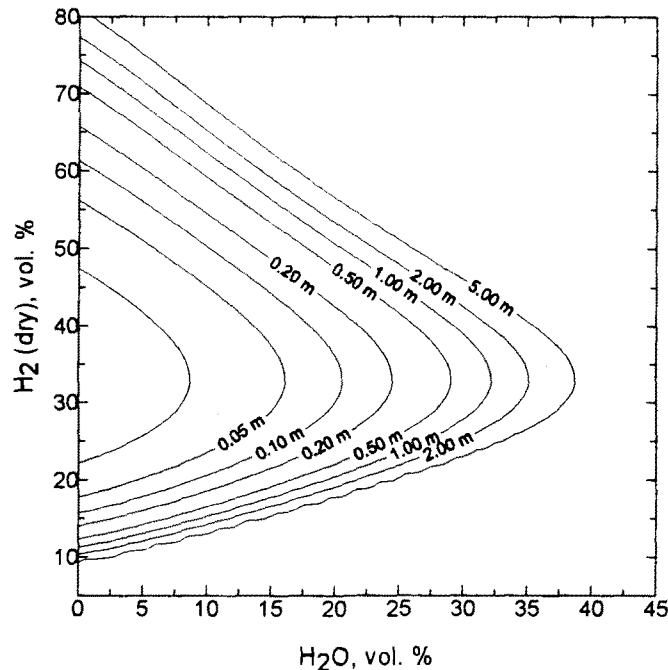


Figure 11. Detonation cell sizes of hydrogen-air-steam mixtures at 375K and atmospheric initial pressure.

Flame Acceleration Limit

DDT criteria described above are useful to estimate possibility of the most dangerous combustion regime. However, fast turbulent deflagrations are not excluded, if DDT is impossible. The blast effect from fast turbulent deflagrations is less severe than that from DDT and detonations, but it can be unacceptable for some containment designs. Fast turbulent deflagrations, as well as DDT cannot be observed, if flame is not able to accelerate. The second important threshold, thus, can be indicated as a flame acceleration limit.

Results of experimental studies on accelerated flames and fast turbulent deflagrations [3] have demonstrated that initial flame acceleration can be followed by deceleration, and even by quenching for lean mixtures. Recent experiments in Brookhaven National Laboratory and Forschungszentrum Karlsruhe show the same effect. It has been shown also, that the increase of turbulence above certain level lead to a decrease of the turbulent burning rate [35]. However no model is available, which is capable to give quantitative description of the flame acceleration limit, similar to that for DDT limit.

Generally, the positive energy feedback responsible for flame acceleration should not be in force under certain conditions in order to destroy the flame acceleration mechanism. An idea can be found in the comparison of the characteristic turbulent mixing time τ_{turb} , and characteristic reaction time in a local flame element τ_r . If $\tau_{\text{turb}} \ll \tau_r$, turbulent mixing is very fast compared to chemical reaction. This means that the flame structure is destroyed, and products become mixed with reactants in a local volume. Temperature controlling reaction rate drops down, the reaction becomes very slow, or is quenched. This qualitative explanation shows that certain increase of turbulence level can be responsible for quenching instead of the increased burning rate. Such a situation is more probable for lean mixtures, which have low reaction rates. The local quenching is able to decelerate overall flame propagation, and even global quenching can occur under certain conditions. Conditions necessary for flame acceleration are defined by the processes of turbulent generation and by the turbulence - flame interaction. Physical mechanisms responsible for quenching phenomena are different from those responsible for detonation formation. This means that sets of defining parameters are different for these processes. Description of mixtures reactivity in terms of the detonation cell size, for example, has no value for the flame acceleration. Presently, the flame acceleration limit remains an unresolved problem. More research are necessary to develop quantitative description of this important phenomena.

Summary

Under certain initial and boundary conditions accelerated flames, fast turbulent deflagrations, and transition to detonation are principally possible in the hydrogen-air-steam mixtures, which can be formed during severe accidents. Fast turbulent deflagration and transition to detonation can result in severe loads to the containment and its internal structures. For such a fast combustion events transient wave processes define the level of pressure loads. Peak overpressures produced by DDT and local detonations are extremely high. Peak values of 10 - 15 bar, and several times more in reflections can be expected for near-limit compositions. Fast turbulent deflagrations result in about twice lower peak values of overpressures compared to that from detonations. Impulse loads to confining structures depend mainly on mixture composition and venting. They are very similar for deflagrations and detonations under the same conditions.

Possibility of DDT for a given mixture composition is limited by geometrical scale. Corresponding criterion has been suggested. Its applicability has been confirmed by analytical, numerical and experimental studies for wide range of mixture compositions, initial and boundary conditions. Detailed studies are being in progress for hydrogen-air-steam mixtures at reactor relevant scale and conditions.

Research on detonations and DDT are able to provide practical outcome for accident analysis, and design needs. More efforts are still necessary to describe limiting conditions for accelerated flames and fast turbulent deflagrations.

Acknowledgments

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5.2 INTERACTION OF TURBULENT DEFLAGRATIONS WITH REPRESENTATIVE FLOW OBSTACLES

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Abstract

In the case of a gradual release of hydrogen in the course of an assumed, severe accident in a light water reactor, the combustion will normally start out as a slow deflagration. Acceleration of an initially slow flame due to interactions of chemical kinetics and turbulent heat and mass transfer can result in very high flame speeds. Therefore, in order to assess hydrogen mitigation techniques, detailed knowledge about flame acceleration and interaction of flames with obstacles is required. The reported investigations are aimed at the investigation of the mechanisms responsible for turbulent flame acceleration and improving present correlations for estimates and models for numerical simulations of hydrogen combustion processes. A medium-scale square cross-section setup is employed, using flow obstacles with shapes representative for reactor containments. The global flame speed is deduced from measurements using thermocouples, pressure transducers and photodiodes. Measurements using a two-component LDA-system are being carried through in order to correlate global flame spread and local turbulence parameters. Results indicate that low blockage-ratio obstacles only marginally influence the flame, as disturbances which are induced remain local to the vicinity of the obstacle and die out very quickly downstream thereof. Flow visualizations by means of a Schlieren setup indicate very complex flow structures in the vicinity of obstacles. The results are being used to validate turbulent reaction models. A model based on probability density functions (pdf) of assumed shape has been developed and initial calculations are presented.

Introduction

Combustion of hydrogen formed in the course of an assumed, severe loss-of-coolant accident is one of the main issues of safety considerations in connection with light water reactors. Igniting the hydrogen before concentrations sensitive to violent combustion modes (e.g. fast deflagrations or even detonations) are reached, is an appealing approach, as the resulting loads on the structure of the containment

remain low and it can be assumed that the hydrogen concentration is considerably decreased if not completely removed. However, open questions in connection with this approach still remain and it is necessary to regard aspects of the combustion which may lead to potential hazards. An area which must be considered in this respect is the influence of turbulence on combustion processes. If the ignition threshold is set sufficiently low, the combustion will start out as a slow deflagration. However, it has been shown [1] that the interaction of chemical kinetics and heat and mass transfer due to turbulent mixing and momentum exchange processes can significantly accelerate flames to limits where damage of the containment structure must be expected.

Hydrogen explosions in confined geometries can be thought of as turbulent flamefronts superimposed on a highly turbulent expansion flow which itself is formed by the heat release due to the combustion and acoustic effects of generated and reflected pressure waves. The pressure increase caused by the flame front depends strongly on its propagation velocity. Immediately after the ignition of an H₂-air mixture, an unstable feed-back mechanism arises. A cellular flame structure develops and generates turbulence ahead of the flame which, by interaction with the flame front, yields turbulent acceleration. In a smooth channel this process can reach a steady state in which the flame front propagates steadily at a constant velocity below the mixture's limit for critical flame speeds with strong precursor shocks. Further flame acceleration is achieved by generation of additional turbulence in the expansion flow ahead of the flame e.g. by interaction of the flow with obstacles and obstructions. It is, therefore, necessary to study the mechanisms which lead to this acceleration and to classify obstacles bound to repeatedly occur in reactor containments according to their influence.

The work carried through in the framework of the present project comprises experimental investigations in order to enhance the insight into the mechanisms of flame acceleration and to obtain a database for validation purposes, as well as theoretical analysis in order to further develop numerical models capable of simulating turbulent combustion processes.

Experimental Investigations

The experiments presently being performed are carried through in a horizontal explosion tube with a square cross-section (268 × 268 mm) and a length of 3.5 m (cf. Fig. 1). Single flow obstacles can be exposed to the propagating flame front in a special tube segment which allows optical access and can be mounted at any position in the sequence of tube elements. The obstacles chosen are representative for typical shapes bound to exist in containments like pipes, pipe bundles, grids, door openings and different half size bodies (cf. Fig. 2).

The instrumentation used to determine global and local propagation information of the flame includes fast thermocouples which record the temperature rise caused

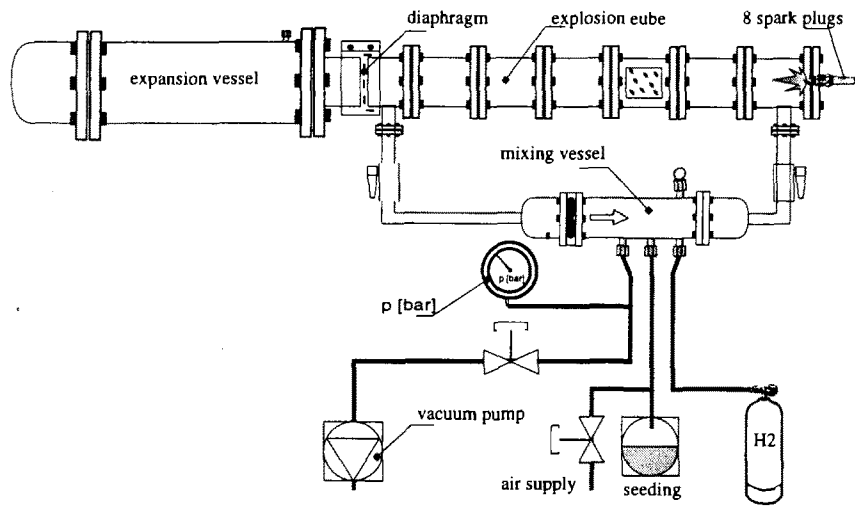


Figure 1. Experimental setup

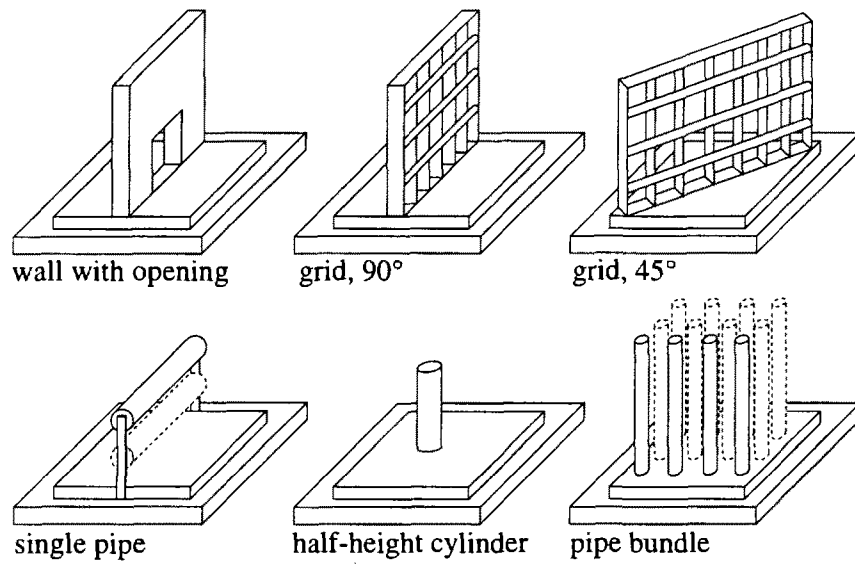


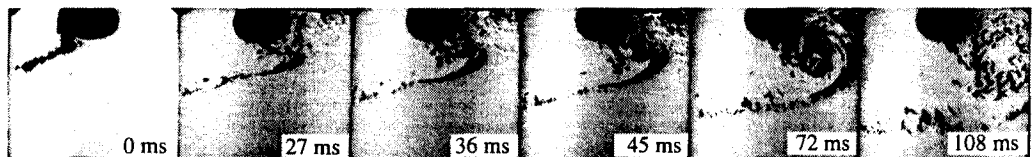
Figure 2. Obstacle configurations



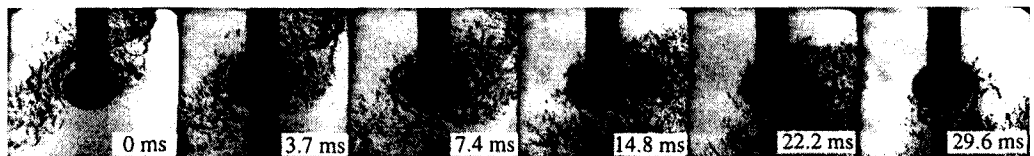
(a) Vertical Half-Height Cylinder, 9 vol.% H₂



(b) Vertical Half-Height Cylinder, 12 vol.% H₂



(c) Horizontal Pipe, Upper Position, 9 vol.% H₂



(d) Horizontal Pipe, Center Position, 12 vol.% H₂

Figure 3. Schlieren ciné records

by the passing flame, positioned at equal spacing along the top and bottom of the tube, in order to account for bouyancy effects. Photodiodes register the light emmitted by the flame and the pressure rise is recorded at two positiones along the tube and, in the case of the wall with an opening, on the obstacle's front and rear face. Local velocity components are determined using a two-component laser Doppler-setup (LDA). High-speed Schlieren cinematography is used to visualize the integral flame structure.

Fig. 3 shows a representative selection of Schlieren records of the flame propagation in the vicinity of a half-height cylinder inserted from the top of the

explosion tube (cf. Figs. 3(a),3(b)) and a horizontal pipe at different positions in the explosion tube (Figs. 3(c) and 3(d)). The flow structures that arise are very complex. The increase in turbulence with increase in H_2 concentration can readily be observed by comparison of eg. Figs. 3(a) and 3(b), showing the vertical half-height cylinder at two different H_2 concentrations. Due to the higher level of turbulence, the 12 vol.% flame does not separate as early at the obstacle and the wake region behind the obstacle is smaller. At 12 vol.%, the flame covers the whole cross section of the explosion tube. The part of the flame that is affected by the obstacle, therefore, is smaller than for the 9 vol.%-flame. In spite of these significant differences, the thermocouple and photo-diode records do not show any difference to the experiments without obstacle.

In the case of the horizontal pipe at the upper position and the 9 vol.%-flame, a phenomenon which is particularly interesting could be visualized. Due to the acceleration of the flow between the obstacle and the upper explosion tube wall,

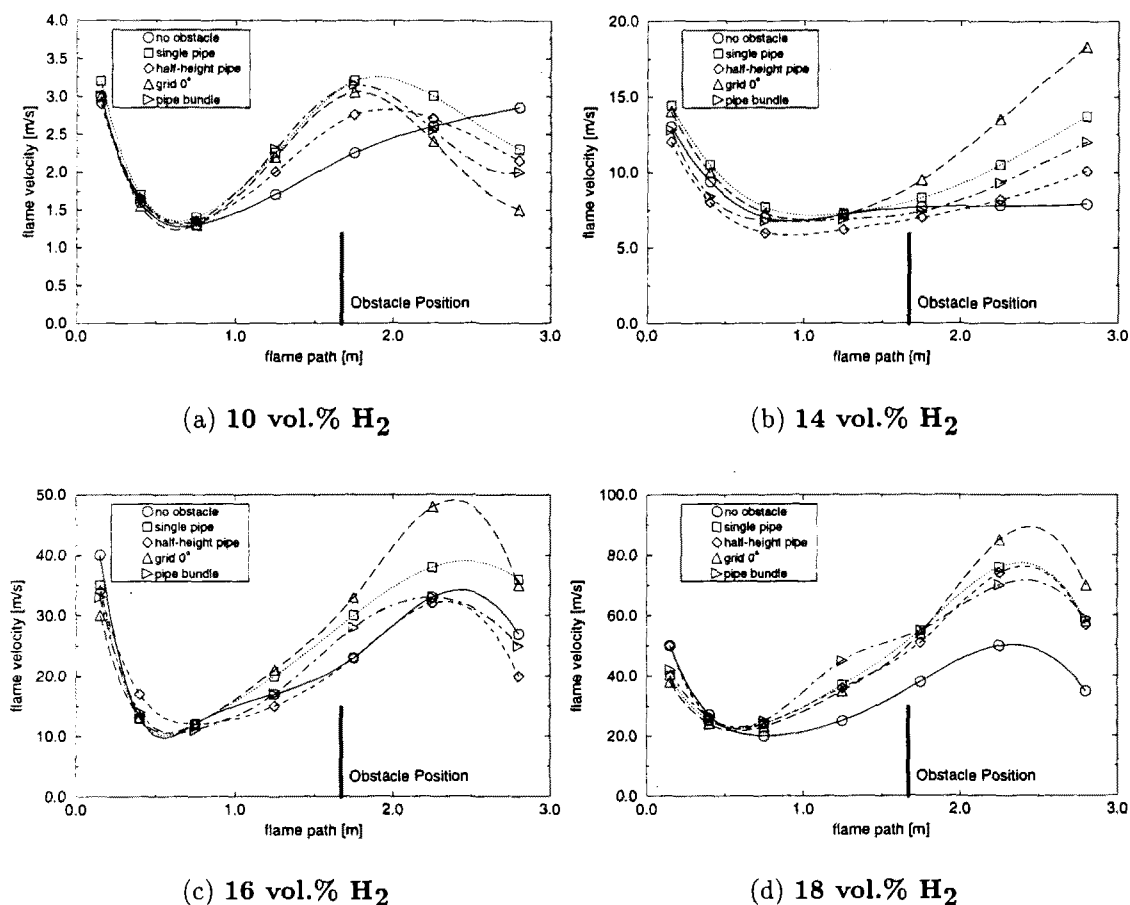


Figure 4. Flame velocity vs. flame path for obstacles with low blockage ratios

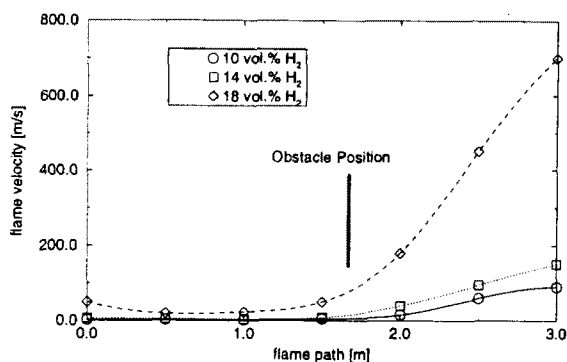


Figure 5. Flame velocity vs. flame path for high blockage ratio obstacle

and interaction with pressure waves, the flame is pushed back at the lower part of the obstacle. Away from the obstacle, the flame burns unobstructed and consequently rolls up to form a large-scale vortex cylinder which is stabilized in the wake of the obstacle. Apparently, it is constantly fed by unburned gas which is entrained along the outer surface of the vortex. In this case the thermocouple measurements along the top of the explosion tube show a slight acceleration of the flame which reaches twice the speed it had at the beginning of the tube (cf. Fig. 4). This situation is quite the same for the case of a 12 vol.-%-flame with the pipe positioned at the center of the explosion tube. The part of the flame which influences the upper row of thermocouples therefore seems to be decoupled from the stagnating vortex.

The respective thermocouple records are shown in Fig. 4. It is found that low blockage ratio obstacles at the investigated positions in the explosion tube do not severely accelerate the flame front to violent combustion modes. The maximum flame speed found at 18 vol.-% H₂ with the grid at 0° inclination does not exceed 90 m/s.

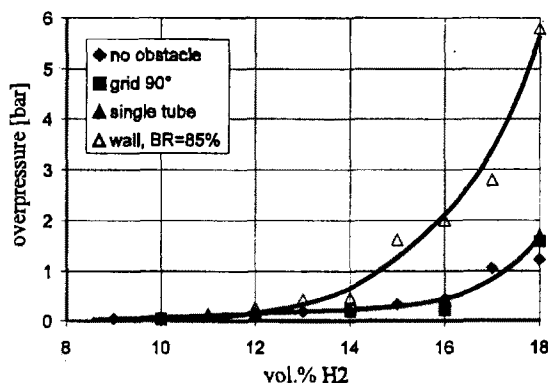


Figure 6. Maximum overpressures for different obstacles

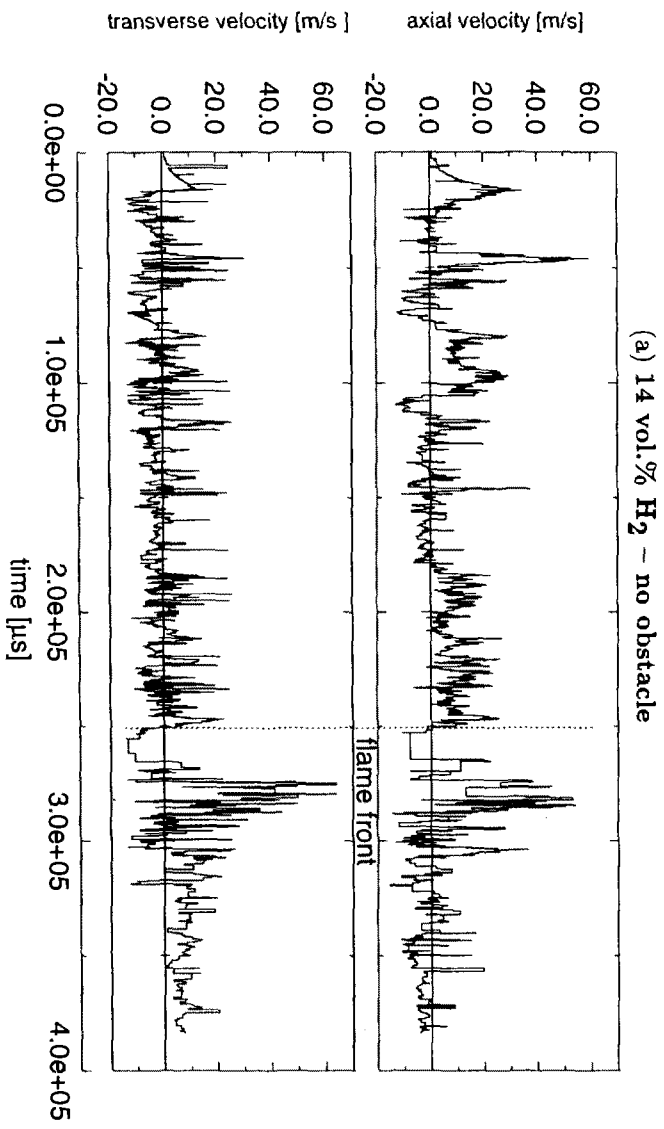
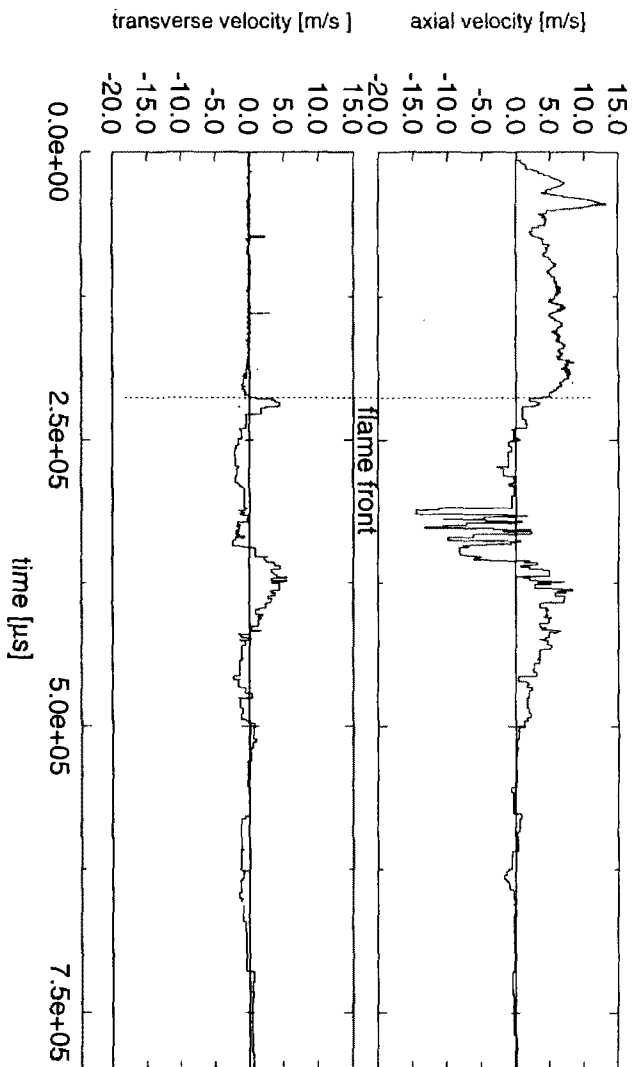


Figure 7. Local velocity samplings using LDA

This situation is completely different for the wall obstacle with a high blockage ratio (85%). In this case, the flow is forced to separate and form a jet behind the obstacle. Flame speeds reached range up to 700 m/s, as shown in Fig. 5, causing maximum overpressures of 6 bar in the tube (cf. Fig. 6). It should be noticed that the generated pressure rises strongly depend on the geometry confining the flame, and that the peak pressure are specific for the applied test setup. Maximum flame speeds appear to be a more consistent criterion for a ranking of obstacles. Comparisons with investigations carried through at BATTELLE GmbH [2] indicate agreement in this respect.

The intense effect of the high blockage ratio wall obstacle can also be seen in the LDA-records. The intense turbulence level is indicated by strong fluctuations of the signal in comparison to the situation without any obstacle (cf. Fig. 7). The velocities also increase strongly. The ratio of turbulent velocity fluctuations to laminar burning speed increases from $\frac{u'}{s_L} \approx 2$ to $\frac{u'}{s_L} \approx 40$.

Numerical Modelling

The aim of enhancing presently existing combustion models is to reach a point where the model is capable of realistically predicting turbulent deflagration processes. It is the fact that the deflagrations under consideration involve highly turbulent expansion flows which, besides the well-known models for the turbulent closure problem, necessitates models also for the chemical reaction rate.

The set of equations which governs a compressible flow problem involving chemical reactions comprises balance equations for mass conservation (continuity):

$$\frac{\partial \rho}{\partial t} + \frac{\partial}{\partial x_i}(\rho u_i) = 0,$$

conservation of species mass fractions (for $r = 1, \dots, N - 1$ species):

$$\frac{\partial(\rho Y_r)}{\partial t} + \frac{\partial}{\partial x_i}(\rho u_i Y_r) = \frac{\partial}{\partial x_i} \left(\Gamma_r \frac{\partial Y_r}{\partial x_i} \right) + R_r,$$

momentum conservation:

$$\frac{\partial(\rho u_j)}{\partial t} + \frac{\partial}{\partial x_i}(\rho u_i u_j) = -\frac{\partial p}{\partial x_j} + \frac{\partial \tau_{ij}}{\partial x_i} + S_{uj},$$

and energy conservation:

$$\frac{\partial(\rho H)}{\partial t} - \frac{\partial P}{\partial t} + \frac{\partial}{\partial x_i}(\rho u_i H) = -\frac{\partial q_i}{\partial x_i} + \frac{\partial u_j \tau_{ij}}{\partial x_i} + S_H,$$

together with equations of state. We consider an ideal gas as a Newtonian fluid, for which:

$$\frac{p}{\rho} = MR_u T,$$

and:

$$\tau_{ij} = \mu \left(\frac{\partial u_i}{\partial x_j} + \frac{\partial u_j}{\partial x_i} \right) + \frac{2}{3} \mu \frac{\partial u_k}{\partial x_k} \delta_{ij}.$$

The total enthalpy H is defined as $H = h + \frac{1}{2} u_i^2$ with h as the static enthalpy from $h = \sum_{r=1}^N Y_r h_r$. The molecular energy transport q_i in direction i is taken as $q_i = -\lambda \frac{\partial T}{\partial x_i} - \sum_{r=1}^N \Gamma_r h_r \frac{\partial Y_r}{\partial x_i}$ and the molecular weight of the mixture is given by $M = \sum_{r=1}^N n_r M_r$.

When using direct numerical simulation (DNS) all length scales of turbulence need to be resolved, and it can be shown that the number N_G of gridpoints required to resolve a three-dimensional geometry is:

$$N_G \sim \left(\frac{Re}{10} \right)^{9/4}.$$

For engineering problems the Reynold's number Re is of the order of $10^3 - 10^6, 10^7$ or even greater. Taking into account that for flows involving chemical reactions the timescales are even smaller, it is evident that DNS presently cannot be regarded as a feasible tool for engineering calculations.

For the above reason, we resort to time-averaging the balance equations according to Reynolds. Instantaneous variables are split into mean and fluctuating part (cf. Fig. 8):

$$\phi = \bar{\phi} + \phi' \quad \text{with} \quad \bar{\phi} = \lim_{T \rightarrow \infty} \frac{1}{T} \int_{t_0}^{t_0+T} \phi dt,$$

or rather, as we are dealing with variable density flows, density weighted time-averaging:

$$\bar{\rho} \tilde{\phi} = \lim_{T \rightarrow \infty} \frac{1}{T} \int_{t_0}^{t_0+T} \rho \phi dt = \overline{\rho \phi} \quad \text{so that} \quad \phi = \tilde{\phi} + \phi''.$$

The averaged balance equations then read:

$$\frac{\partial \bar{\rho}}{\partial t} + \frac{\partial}{\partial x_i} (\bar{\rho} \tilde{u}_i) = 0,$$

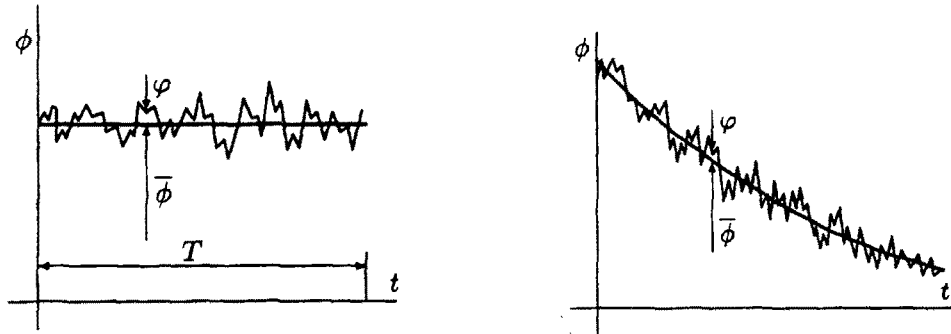


Figure 8. Time-averaging procedure for steady and transient flows

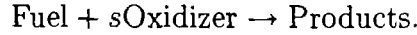
$$\frac{\partial \bar{\rho} \tilde{Y}_r}{\partial t} + \frac{\partial}{\partial x_i} (\bar{\rho} \tilde{u}_i \tilde{Y}_r) = \frac{\partial}{\partial x_i} \left(\Gamma_r \frac{\partial \tilde{Y}_r}{\partial x_i} - \overline{\rho u_i'' Y_r''} \right) + \bar{R}_r; \quad r = 1, \dots, N-1, \quad (1)$$

$$\frac{\partial (\bar{\rho} \tilde{u}_j)}{\partial t} + \frac{\partial}{\partial x_i} (\bar{\rho} \tilde{u}_i \tilde{u}_j) = -\frac{\partial \bar{p}}{\partial x_j} + \frac{\partial}{\partial x_i} (\bar{\tau}_{ij} - \overline{\rho u_j'' u_i''}) + \bar{S}_{uj},$$

$$\begin{aligned} \frac{\partial (\bar{\rho} \tilde{H})}{\partial t} - \frac{\partial \bar{P}}{\partial t} + \frac{\partial}{\partial x_i} (\bar{\rho} \tilde{u}_i \tilde{H}) = \\ - \frac{\partial}{\partial x_i} (\bar{q}_i + \overline{\rho u_i'' h''}) + \frac{\partial}{\partial x_i} \left(\tilde{u}_j (\bar{\tau}_{ij} - \overline{\rho u_j'' u_i''}) + \overline{u_j'' \tau_{ij}''} \right) + \bar{S}_H. \end{aligned}$$

The closure problems arise from the cross-correlation terms $\overline{\rho u_j'' u_i''}$, $\overline{\rho u_j'' h''}$ and $\overline{u_j'' \tau_{ij}''}$ which are not related to mean values and, therefore, represent additional unknowns for the set of equations. As a turbulence model to close the set of equations with respect to these terms, we use the well-known k - ϵ model with enhancements to consider compressibility.

The present contribution, however, will not focus on the details of turbulence modelling, but rather on the closure problem related to the chemical source term $\bar{R}_r$ in (1). We regard a global one-step irreversible reaction of fuel and oxidizer to products:



The instantaneous reaction rate for the fuel species of this reaction can be written in terms of fluctuating quantities as:

$$R_{fu} = M_{fu} \left(\frac{\rho Y_{fu}}{M_{fu}} \right) \left(\frac{\rho Y_{ox}}{M_{ox}} \right)^s A_1 e^{-E_1/RT}. \quad (2)$$

However, as this equation is highly nonlinear, its mean value $\bar{R}_{fu}$ which is required in Eq. (1) for the fuel species cannot be estimated simply by introducing mean values into this equation:

$$\bar{R}_{fu} \neq M_{fu} \left(\frac{\bar{\rho} \bar{Y}_{fu}}{M_{fu}} \right) \left(\frac{\bar{\rho} \bar{Y}_{ox}}{M_{ox}} \right)^s A_1 e^{-E_1/R\bar{T}},$$

but rather from

$$\bar{R}_{fu} = M_{fu} \left(\frac{\bar{\rho} \bar{Y}_{fu}}{M_{fu}} \right) \left(\frac{\bar{\rho} \bar{Y}_{ox}}{M_{ox}} \right)^s A_1 e^{-E_1/R\bar{T}} (1 + F),$$

where F represents a polynomial of cross-correlations of T'' , Y_{fu}'' and Y_{ox}'' . Failing to consider this term can lead to errors up to three orders of magnitude [3]. The number of correlations involved, prohibits trying to model every single term. It is therefore necessary to model the chemical reaction rate as a whole. Approaches to this problem include semi-empirical combustion models such as the Eddy-Breakup model of Spalding [4] or the Eddy-Dissipation-Concept of Magnussen and

Hjertager [5], flamelet approaches [6] and more recently models employing probability density functions (pdf) [7]. The latter can be distinguished into two groups, one which solves transport equations for the pdf and the other which precibes a shape of the pdf. For the present work we use pdfs of assumed shape.

The probability density $P(\phi)$ is defined such that $P(\phi)d\phi$ is the probability that ϕ lies in a small range $d\phi$ around a particular function $\phi(x_i, t)$. If ϕ depends on various variables $\rho, T, Y_N, \dots$, the mean value of ϕ can then be calculated as:

$$\bar{\phi}(x_i, t) = \int_{\rho} \int_T \int_{Y_N} R_n(\rho, T, Y_N, \dots) \bar{P}(\rho, T, Y_N, \dots; x_i, t) d\rho dT dY_N \dots$$

In the above equaton, $P(\rho, T, Y_N, \dots; x_i, t)$ denotes the joint pdf depending on $\rho, T, Y_N, \dots$. In our case we reduce the dependency by defining:

$$\rho = f_1(Y_p), \quad T = f_2(Y_p), \quad Y_{fu} = f_3(Y_p) \quad \text{and} \quad Y_{ox} = f_4(Y_p). \quad (3)$$

and a variable c that specifies the reaction progress:

$$c = \frac{Y_p}{Y_{p,\infty}}. \quad (4)$$

Using Eqs. (3) and (4), Eq. (2) can be simplified so that the reaction rate is merely a function of c :

$$R_n(\rho, T, Y_N, \dots) \rightarrow R_n(c).$$

As c is defined only for $0 \leq c \leq 1$, the mean reaction rate can then be calculated from:

$$\bar{R}_n = \int_0^1 R_n(c) P(c; x_i, t) dc.$$

As already mentioned, the shape of $P(c; x_i, t)$ is assumed in the present work. It has been shown [3] that the results of pdf calculations are relatively insensitive to the precise shape used. As a natural approach, we use a Gaussian distribution with the parts extending outside the range of c clipped into Dirac functions at the bounds, as shown in Fig. 9.

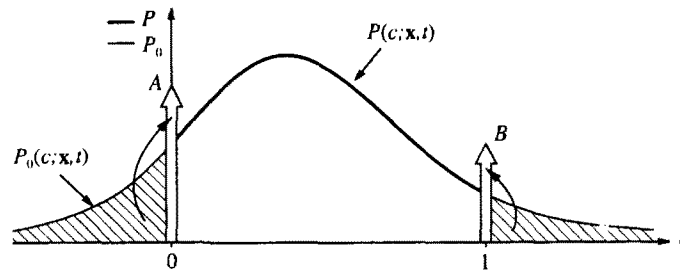


Figure 9. Clipped Gaussian distribution

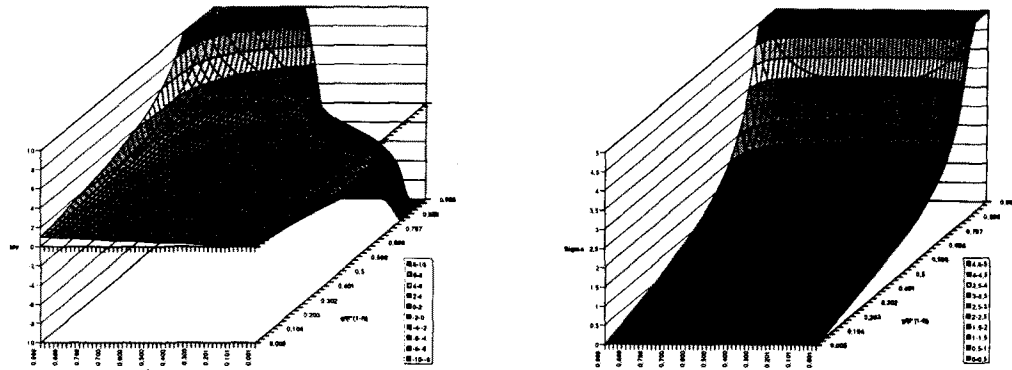


Figure 10. Tables for μ and σ

This functions is defined as:

$$P(c; x_i, t) = P_0(c; x_i, t) [H(c) - H(c - 1)] + A(x_i, t)\delta(c) + B(x_i, t)\delta(1 - c),$$

with:

$$P_0(c; x_i, t) = \frac{1}{\sigma\sqrt{2\pi}} \exp \left[-\frac{1}{2} \left(\frac{c - \mu}{\sigma} \right)^2 \right],$$

where μ and σ denote the mean and variance of the distribution, respectively. These two parameters can be determined from $\tilde{c}$ and $\tilde{c}'^2$, as a scale for the mean of the fluctuations of c , for which additional balance equations are solved. The clipped Gaussian distribution has the drawback that, given $\tilde{c}$ and $\tilde{c}'^2$, it cannot explicitly be solved for μ and σ . Some root-finding algorithm must be employed, which for the number of gridpoints involved, requires substantial computational time. For

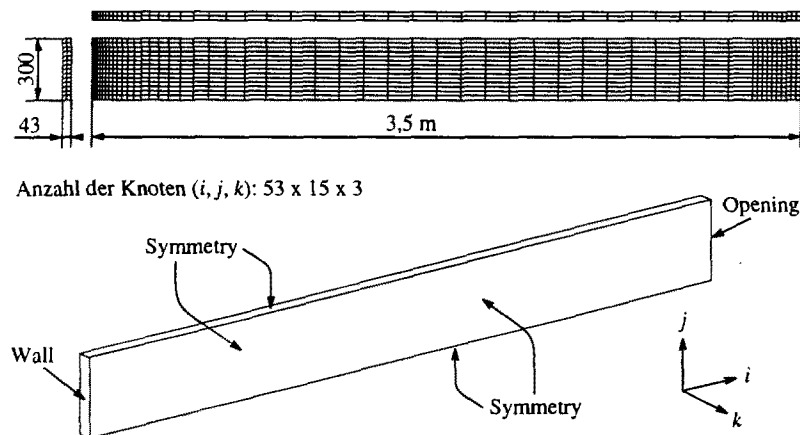
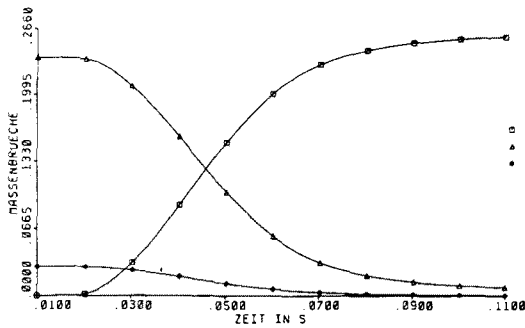
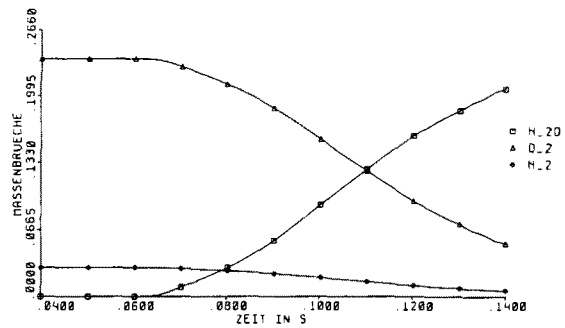


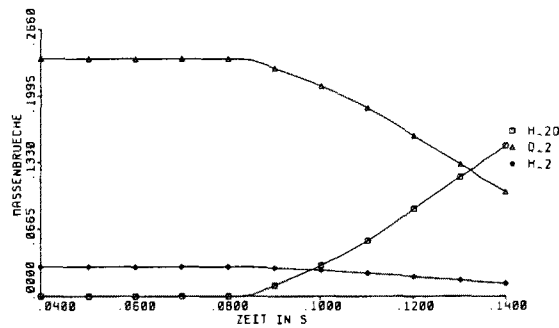
Figure 11. Computational grid and boundary conditions



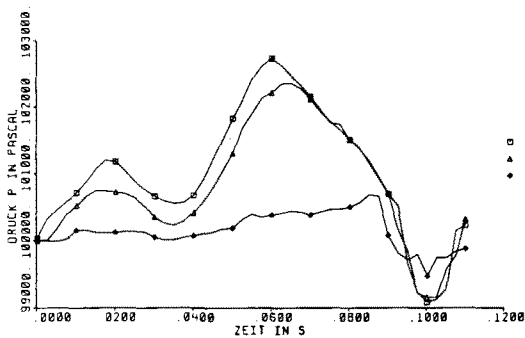
(a) massfractions at $x = 0.1$ m vs. time



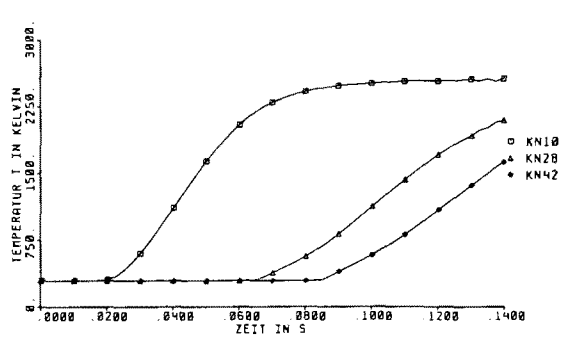
(b) massfractions at $x = 1.5$ m vs. time



(c) massfractions at $x = 3.3$ m vs. time



(d) pressures at the 3 locations vs. time



(e) temperatures at the 3 locations vs. time

Figure 12. Results of calculations

this reason, these two values are tabulated following [8], as shown in Fig. 10. The required values can then be interpolated during the calculation from the tables.

The computational domain that is used for the initial calculations is shown in Fig. 11. We use $53 \times 15 \times 3$ points in the x , y and z -direction. The pdf-model was implemented into a commercial CFD-code using user-definable routines. Fig. 12 shows results of the calculations. The calculations are in agreement with the experimental findings for the case without any obstacle. Main characteristics of the combustion process such as the double pressure rise or the timescale of the rise in temperatures are predicted well.

Concluding Remarks

The experimental investigations carried through in the course of the present project have shown that for low blockage ratio obstacles in the square cross-section tube, the influence of the obstacles remains very limited. Additional turbulence is generated mainly in the vicinity of the obstacle and dies out very quickly downstream of the obstacle. High blockage ratio obstacles such as a simulated wall with an opening or walking grids inclined to the flow, however, lead to accelerations of the flame. From comparison with measurements carried through in smaller-scale setups, it was found, that the maximum flame speed is a more consistent criterion for ranking obstacles according to their influence on the flame than the peak pressure, as the latter depends strongly on the geometry of the confinement rather than on the obstacle. In the further course of the project visualizations of the reaction zone using laser induced (predissociation) fluorescence will be carried through, in order to be able to analyse the interaction of turbulence and chemical reaction precisely.

A closure model for the chemical reaction rate based on probability density functions of assumed shape has been developed and implemented into a commercial CFD-code. The shape assumed for the pdf is that of a clipped Gaussian distribution. A reaction progress variable is used so that a single variable pdf can be employed. Calculation of the Gaussian distribution is speeded up by precalculating the dependencies and using a table look-up procedure during the calculation. The results of initial calculations show agreement with experimental findings. The model will be extended to multidimensional pdfs, in order to allow for more complex chemical reaction mechanisms.

Acknowledgements

It is gratefully acknowledged that the work presented in this paper has been supported by the German Ministry of Education, Science, Research and Technology.

Nomenclature

A_1	reaction rate constant
c	reaction progress variable
E_1	reaction rate constant
H	total enthalpy, Heaviside function
h	static enthalpy
M	molar weight
n	number of moles
N	number species
q_i	molecular energy transport in i th direction
s	burning velocity
t	time
r	reaction rate
R	gas constant
s	stoichiometric coefficient
S	additional source terms in balance equations
T	Temperature
u_i	instantaneous velocity in i th direction

u'_i	velocity fluctuation in i th direction
x_i	i th coordinate direction
Y	mass fraction

Greek Symbols

Γ	effective molecular diffusion coefficient
δ_{ij}	unit tensor
δ	Dirac-peakfunction
μ	dynamic viscosity
ρ	density
ϕ	representative scalar
τ_{ij}	viscous stress tensor

Subscripts

fu	fuel
L	laminar
ox	oxidizer
r	r th chemical species
u	universal
∞	completely burned state

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5.3 COMBUSTION OF NON-UNIFORM HYDROGEN-AIR MIXTURES

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Abstract

The consequences of a hydrogen burn inside the containment building are usually assessed using the assumption that the flammable mixture is uniformly distributed. The implications of this assumption on the overall containment thermalhydraulic response have not been examined in detail. The present study examines the pressure development resulting from vented combustion of stratified mixtures of hydrogen in air. Mixtures were burned in a 10.7 m³ CTF cylinder and vented to a 6.3 m³ sphere. Hydrogen was stratified in sensitive mixtures at the top of the cylinder and burned; the resulting overpressures were compared to those obtained from uniform mixtures having the same quantity of hydrogen. Results showed that the peak overpressure was not significantly affected when gases were stratified. However, burn time and the extent of combustion were affected in a manner similar to the effects of turbulence.

Introduction

During postulated loss of cooling accidents in nuclear reactors, large amounts of hydrogen can be released into the reactor containment building. Deliberate ignition is a common approach to mitigating the consequences of a hydrogen deflagration. This approach is based on the assumption that the gas mixture can be burned near its flammability limit, avoiding the potential for a large pressure rise and strong blast waves. Much of the analysis performed to support this technique is based on combustion behaviour in uniform mixtures [1,2,3]. Under some circumstances, scenarios can be postulated where non-uniform mixtures form. For example, mixing experiments in the German HDR test facility [4] showed stratification of gases due to thermal effects. Recent work has examined constant volume combustion in hydrogen-air gradients [5] and hydrogen-air-steam gradients [6]. The conclusion of both of these pieces of work was that the presence of gradients can increase the extent of combustion, particularly for low hydrogen concentration mixtures, and significantly increase the resulting overpressure. However, the experimentally observed peak pressures were still bounded by the constant volume combustion pressure. The purpose of the present study is to compare the behaviour of combustion in non-uniform mixtures of hydrogen and air to combustion in equivalent quantities of uniformly distributed gases.

Experimental Facility and Procedure

This test program was conducted in the Combustion Test Facility (CTF) at AECL Whiteshell Laboratories. Fig. 1 shows a schematic of the two pressure vessels used for this study. The first of these was the 10.7-m³ cylinder which is 6 m high and 1.5 m in diameter. The second vessel is the 6.3-m³ sphere, which is 2.3 m in diameter. A 3-m long pipe with an inside diameter of 0.45 m connects the two vessels. The entire system is rated to a pressure of 10 MPa.

An aluminum foil burst disk was mounted between the connecting pipe and the sphere to isolate the two vessels. The aluminum foil was ripped by pulling a thin wire across it just prior to ignition. The ripped disk offered virtually no resistance to the pressure driven flow between the vessels. This system allowed the creation of completely independent initial conditions in each of the two vessels.

Stratified mixtures were created in two steps. First, the total amount of hydrogen was added by partial pressure to the cylinder side of the initially evacuated system. Air was added until the concentration of hydrogen in air was at the level desired for the stratified layer. Second, after these initial gases were thoroughly mixed, additional air was slowly added to the quiescent mixture through an inlet at the bottom of the cylinder. This addition continued until the average concentration of hydrogen in air in the vessel reached the desired value. The result of this process is a layer of high concentration hydrogen in air floating on a layer of air.

Piezoelectric and strain gauge pressure transducers were mounted at different locations in the two vessels. Fine wire (75 μ m, type S) thermocouples were used to detect flame arrival at various locations. Concentrations were measured from eleven probe locations in the cylinder, one location in the connecting pipe and two locations in the sphere. Concentration measurements were made prior to each experiment to determine the initial distribution of hydrogen and following each experiment to determine the extent of combustion.

Results and Discussion

Constant Volume Experiments

Previously, a series of experiments was conducted to examine the behaviour of combustion in stratified mixtures in a constant volume. These experiments were performed in the unvented CTF cylinder [5]. Peak pressures obtained from ignition of stratified and well-mixed gases are plotted against average hydrogen concentration in Fig. 2. This figure also shows the values calculated for the constant volume combustion pressure. The pressures obtained from stratified mixtures were generally equal to or lower than the overpressures obtained from burning the same mixtures under well-mixed conditions. Uniform mixtures with average concentrations below the downward flammability limit burned very little of the vessel contents. However, when the same quantity of hydrogen was stratified at the top of the vessel in a layer with an average concentration higher than the downward propagation limit, a

large fraction of the available hydrogen was consumed. The resulting overpressure was much larger than what was observed when trying to burn well-mixed gases of the same average concentration. However, these overpressures were bounded by the constant volume combustion pressures of the average mixtures. The most significant difference between deflagrations in non-uniform and well-mixed experiments was the rate of pressurization; when hydrogen was stratified and ignited at the top of the vessel, peak pressures were reached in a fraction of the time obtained from burning equivalent well-mixed gases.

The effect of stratification is similar to the effect of turbulence on burn time. Constant volume combustion experiments in the CTF sphere [7] with fan turbulence showed that the effects were most pronounced for mixtures with low hydrogen concentrations (<10%) where both the rate and the extent of combustion are increased. In general, a quiescent mixture with an average concentration of 6% hydrogen burns very little fuel when ignited. However, a significant amount of fuel was burned when the same amount of hydrogen was stratified in a layer of 12% hydrogen at the top of the cylinder. The same is true when a 6% mixture was burned in the sphere in the presence of fan turbulence. The value for time to peak pressure obtained from the gradient experiment in the cylinder had a similar order of magnitude as the value obtained from the fan turbulence experiment in the sphere.

Vented Experiments

A series of experiments was conducted to examine the behaviour of combustion in stratified mixtures in a vented volume. Test mixtures were burned in the CTF cylinder and vented into the CTF sphere which initially contained only air. Mixtures with average concentrations of 6%, 10% and 15% hydrogen relative to the volume of the cylinder and distributions ranging from uniform to stratified layers containing up to 60% hydrogen by volume were tested.

Peak Pressure in Vented Combustion

Vented experiments with well-mixed gases in the cylinder were performed to establish a benchmark against which burning in stratified mixtures could be compared. Fig. 3 shows pressure traces from the cylinder and the sphere during a vented burn of 10% well-mixed hydrogen. The two vessels pressurize at the same rate, and reach the same peak pressure at the same time.

Fig. 4 shows measured pressure transients from the cylinder and the sphere for a stratified mixture with an average concentration of ten percent hydrogen in air. In this case, the hydrogen was stratified in a layer having a concentration of 30% hydrogen in air. The pressure-time history is very different from that observed from the burning of the same quantity of well-mixed hydrogen. Both vessels pressurize in a fraction of the time taken by the well-mixed case. The cylinder reached its peak pressure before much pressure rise was observed in the sphere. The pressure rise in the sphere lags the pressure rise in the cylinder by about 0.02 s, the time required for a pressure wave to travel from one vessel to the other. Oscillations in the cylinder pressure trace developed at the time the flame reached the vent. The arrival time was verified by measurements from thermocouples adjacent to the vent. Solberg [8] observed similar oscillations in pressure traces when the flame reached the vent in

experiments with propane in a vented 35 m³ test volume. These are known as Taylor instabilities and are generated by the acceleration of higher density unburnt gas toward the vent when the volume of burnt gas initially decreases due to venting. The instabilities quickly increased the overall burning and pressurization rate. Very large fluctuations in the cylinder pressure begin at about 0.04 s. These are acoustic in nature and their frequency and magnitude depends on the transducer location and orientation. In this case the frequency corresponds to the characteristic transverse frequency of the cylinder. Similar oscillations can also be seen in the pressure trace from the sphere. Pressure increases due to oscillations were not used to determine peak overpressures. Peak values were determined by a combination of comparing results from different transducer locations and taking the mean of the oscillations.

Fig. 5 shows peak pressures obtained from deflagrations in the vented cylinder. The experiments have average concentrations of 6%, 10% and 15%. The peak pressure values are plotted as functions of the concentration at the igniter location in the cylinder. These concentration values range from the value for well-mixed gases to mixtures with hydrogen stratified in layers with up to 60% hydrogen in air.

Stratification did not have a strong effect on the overpressures observed in the cylinder for mixtures with average concentrations of 6%. However, more sensitive mixtures showed a pressure increase when hydrogen was stratified in a sensitive layer as opposed to being uniformly mixed in the test vessel. The largest increase in overpressure was observed in experiments with stoichiometric hydrogen at the igniter location in the cylinder. A 24.4% increase in peak overpressure was observed for mixtures with average concentrations of 10% hydrogen in air. A 22.7% increase in peak overpressure was observed for mixtures with average concentrations of 15% hydrogen in air. In both of these cases the peak overpressure was reached in the cylinder before much pressure rise had occurred in the sphere. The increase in peak overpressure due to stratification was still bounded by the constant volume combustion pressure.

The results from these experiments suggest that, for this configuration, the largest increase in overpressure possible from the deflagration of a stratified mixture with an average concentration of 15% or less is about 25% greater than the peak pressure observed in well-mixed gases. This is the increase in overpressure obtained for the most severe condition possible, a dry mixture with hydrogen stratified in a 30% layer at the top of the vessel.

Time to Peak Pressure in Vented Volumes

Results from constant volume experiments [5] showed that stratified mixtures burn much faster than well-mixed gases having the same average concentration of hydrogen. The same is true for vented experiments. Fig. 6 shows the time to peak pressure for stratified mixtures with 6%, 10% and 15% average hydrogen concentrations. The laminar burning velocities of these mixtures are 0.03 m/s, 0.2 m/s, and 1.0 m/s, respectively. The times to peak pressure are plotted as a function of the hydrogen concentration at the igniter. All of the stratified mixtures burn in a fraction of the time required to burn the equivalent quantity of well-mixed gas. For example, the most sensitive well-mixed experiment, having an average concentration of 15%, took 0.5 s to reach peak pressure. The same mixture stratified in a 30% layer

reached peak pressure in only 0.05 s. In fact, the times to peak pressure for the stratified mixtures with concentrations at the igniter between 20 and 70 percent all have values between 0.04 s and 0.10 s. Closer inspection of the time to peak pressure suggests that it is an inverse function of the burning velocity of the mixture concentration at the igniter.

Although the rapid pressurization of the cylinder did not result in a significant pressure increase over measurements from well-mixed experiments, the effect of stratification on burn time has significant implications for the analysis of post-accident containment response. The tools used to predict deflagrations in post-accident atmospheres assume well-mixed gases in sub-volumes. Some of the lumped parameter models presently in use require the specification of a burn time based on the average mixture composition as an input parameter [9].

As mentioned earlier, the effect of stratification on burn time is similar to the effect of turbulence. Lumped parameter calculations often rely on an enhancement factor, defined as some function of the ratio of turbulent burning velocity (S_T) to laminar burning velocity (S_L), to account for increases in average flame speed due to turbulence [9,10]. For a 10% average hydrogen concentration in the cylinder, the reduction in burn time from stratifying hydrogen in a 30% layer is equivalent to a turbulence enhancement ratio, S_T/S_L , of 66. The enhancing effect of stratification is reduced as the average concentration of a mixture is increased. For a 15% average hydrogen concentration in the cylinder, the reduction in burn time from stratifying hydrogen in a 30% layer is equivalent to a turbulence enhancement ratio of 7.2.

Conclusions

The results from these experiments suggest the effects of stratification on combustion pressure are similar to the effects of turbulence. At low concentrations, the extent of combustion is increased and the burn time is significantly reduced by stratification. At higher concentrations, the peak overpressure from burning stratified gases in a vented volume is not significantly higher than burning the same quantity of well-mixed gases. However, there is still a significant reduction in the burn time. This reduction may have implications for modelling the containment thermalhydraulic response using lumped parameter analysis. These approaches generally require the direct specification of a burn time based on the average mixture composition or the specification of an enhancement factor to account for turbulence effects. It may be possible to interpret the effects of stratification on combustion using an approach similar to that used for turbulence.

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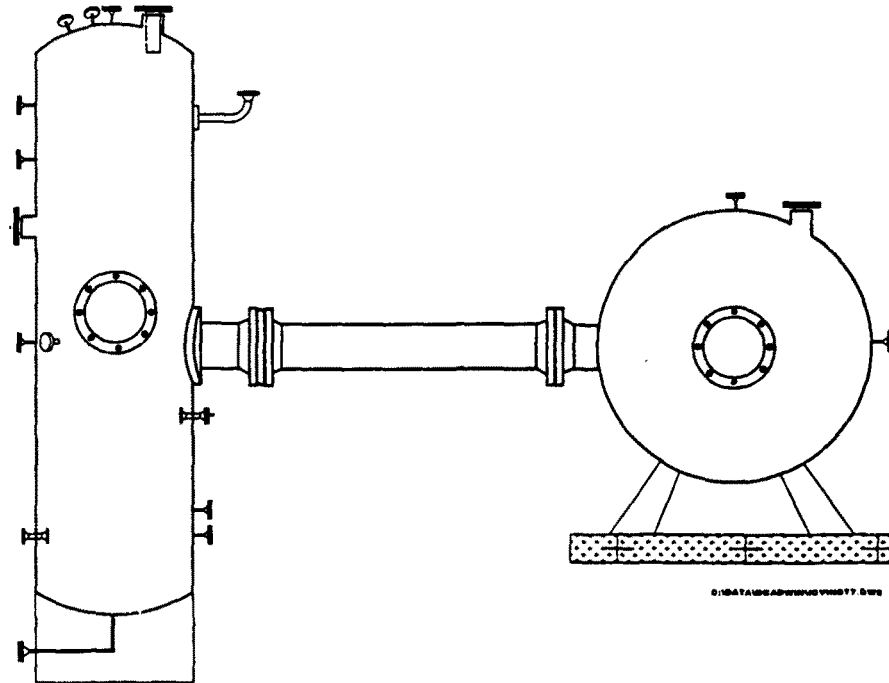


Figure 1. The CTF facility configured for vented cylinder experiments.

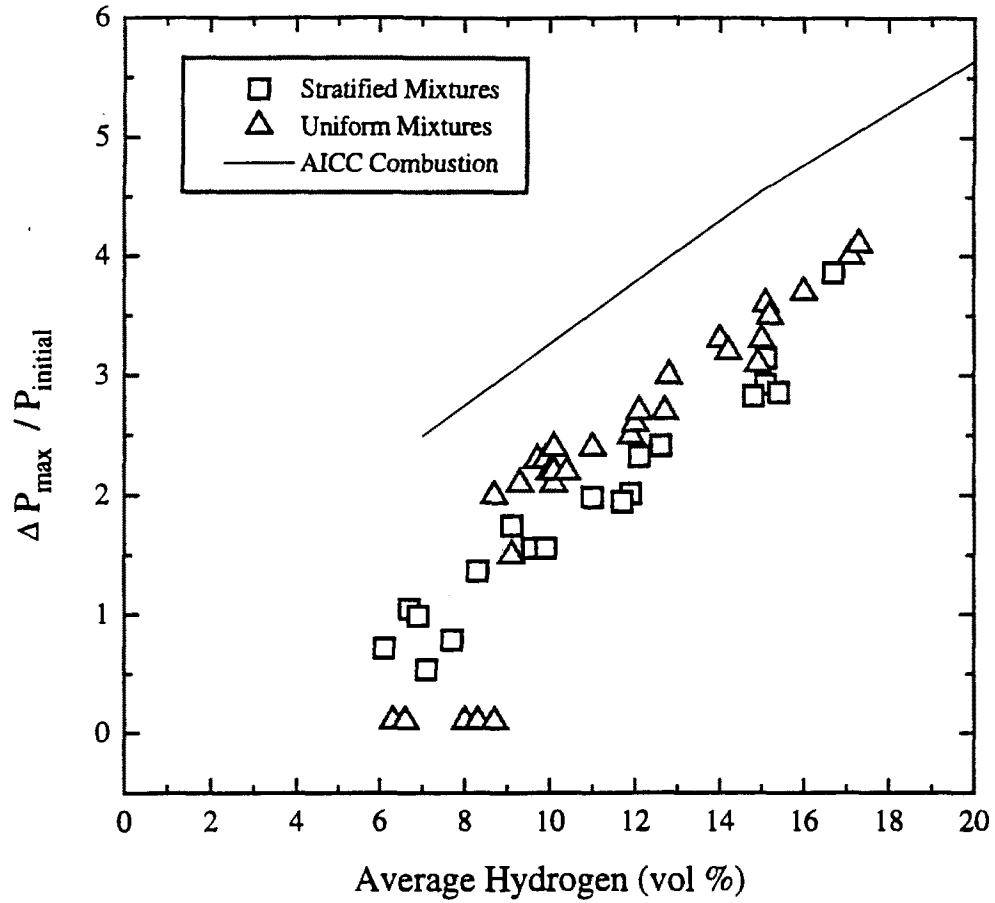


Figure 2. Constant volume combustion overpressure ratios for stratified gases and well-mixed gases ignited at the top of the cylinder (AICC = Adiabatic Isochoric Complete Combustion).

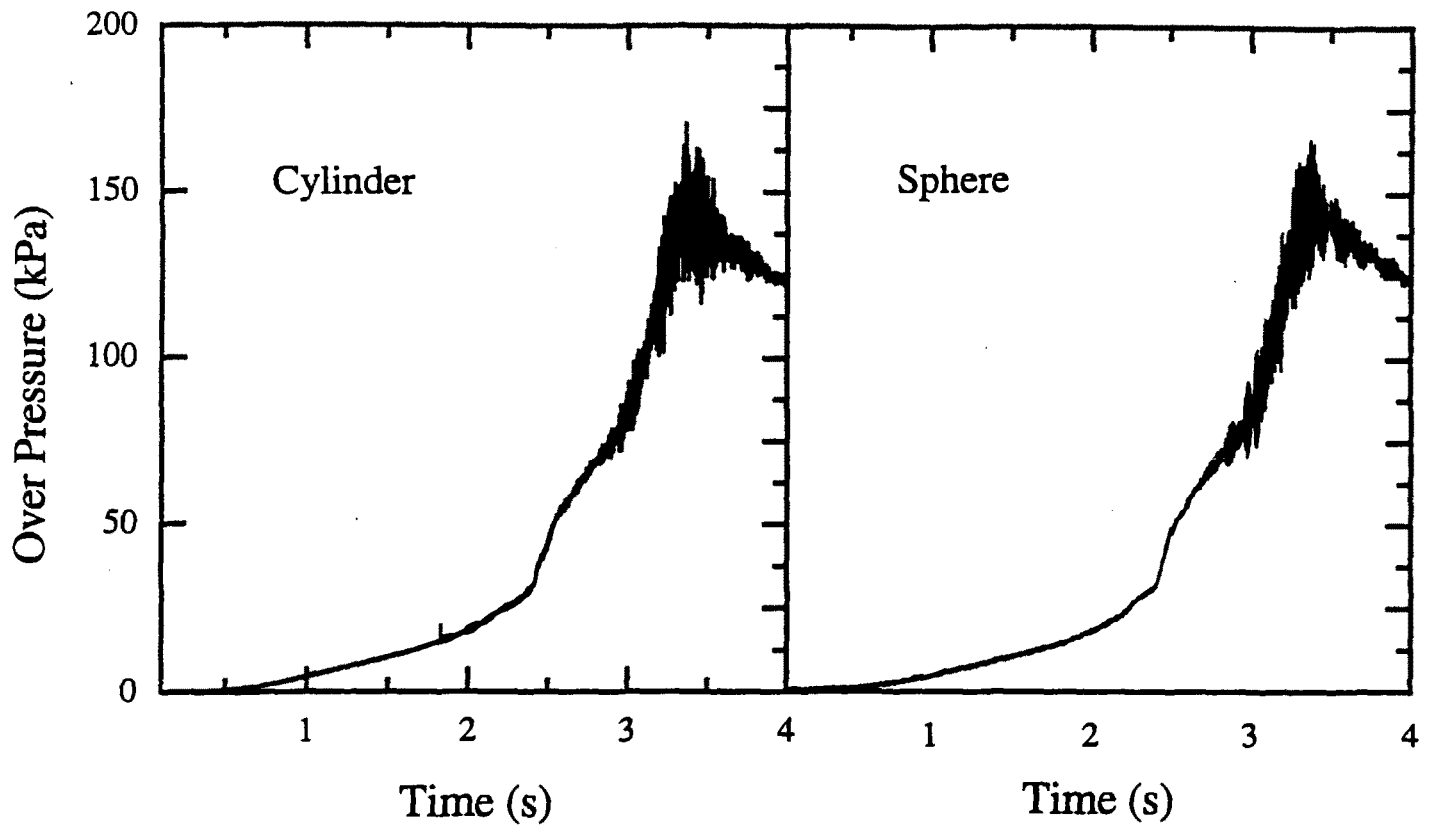


Figure 3. Transient pressure in the cylinder (left) and the sphere (right) for a 10% well-mixed hydrogen burn in the cylinder.

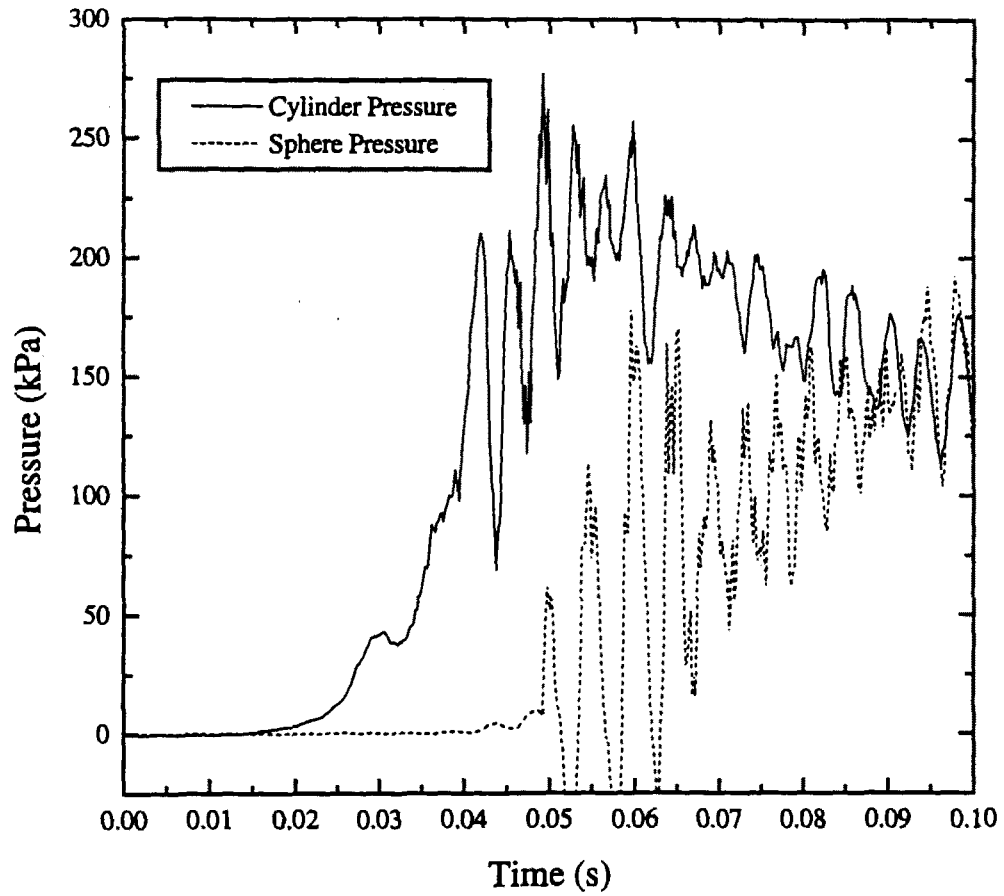


Figure 4. Transient pressure in the cylinder and the sphere for a 10% average concentration mixture with hydrogen stratified in a 30% layer and ignited at the top of the cylinder.

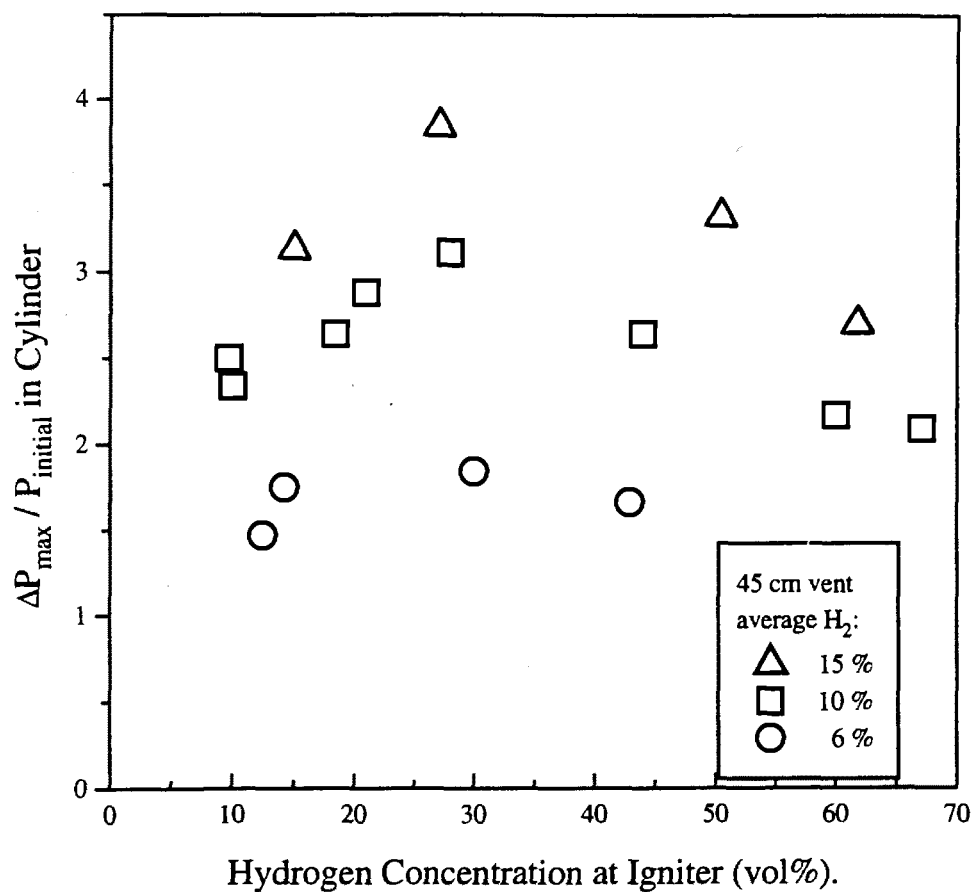


Figure 5. Peak overpressure in the cylinder plotted against stratified hydrogen concentration at the igniter for mixtures with average concentrations of 6%, 10% and 15% hydrogen in air.

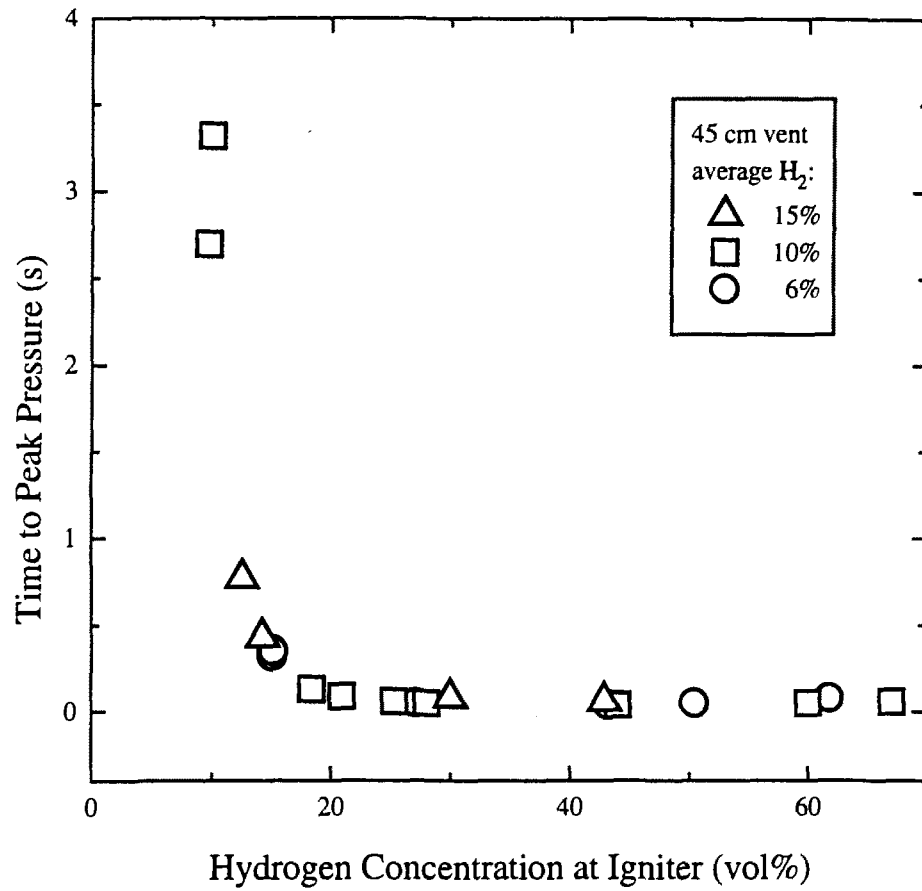


Figure 6. Time to peak pressure plotted against stratified hydrogen concentration at the igniter for mixtures with average concentrations of 6%, 10% and 15% hydrogen in air.

5.4 THE STRUCTURE OF HORIZONTAL HYDROGEN-STEAM DIFFUSION FLAMES

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Abstract

This paper summarizes a systematic study on the stability, peak temperature and flame length of various horizontal hydrogen-steam diffusion flames in air. Results from this study are discussed in terms of their impact on hydrogen management in a nuclear containment building after a nuclear reactor accident. They show that, for a certain range of emerging hydrogen-steam compositions, a stable diffusion flame can anchor itself at the break in the primary heat transport system. The length of this flame can be up to 100 times the break diameter. This implies that creation of a stable diffusion flame at the break is a possible outcome of the deliberate ignition mitigation scheme. The high temperature and heat flux from a diffusion flame can threaten nearby equipment. However, due to the presence of steam and turbulent mixing with surrounding air, the peak temperatures of these diffusion flames are much lower than the adiabatic constant pressure combustion temperature of a stoichiometric hydrogen-air mixture. These results suggest that the threat of a diffusion flame anchored at the break may be less severe than conservative analysis would indicate. Furthermore, such a flame can remove hydrogen at the source and minimize the possibility of a global gas explosion.

Introduction

During a postulated loss-of-coolant accident in a CANDU[®] reactor, high-temperature hydrogen-steam mixtures emerging from a break in the reactor's primary heat transport system are released into the containment building and can accumulate to flammable concentrations. The ignition of the flammable mixture can result in a flash back and the formation of a turbulent diffusion flame at the break. Therefore, the creation of a diffusion flame at the break can be a direct outcome of the deliberate ignition scheme. It should be mentioned that a discrete ignition source is not always needed. The emerging mixture can also auto-ignite if its temperature is sufficiently high. For a dry hydrogen jet, the auto-ignition temperature was found to be about 680°C [1]. The effects of such a diffusion flame in the containment building are twofold. First, it can remove hydrogen (or deuterium) as it leaves the break, thus minimizing the potential of a global gas explosion inside the containment building. Second, the high thermal load from a diffusion flame can potentially threaten the survivability of nearby safety equipment. To fully assess the

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consequences of the deliberate ignition scheme, it is necessary to determine whether a stable diffusion flame can form at a break. If a flame does form, it is necessary to estimate the thermal load on nearby objects for various postulated accident scenarios. A general review on the structure and heat transfer of various types of diffusion flames relevant to a nuclear reactor accident were reported by Shepherd [2]. Most of the reported data were on vertical dry hydrogen flames. Data for non-vertical flames involving hydrogen-steam mixtures, particularly for mixtures with high steam content, were limited. This paper summarizes the results of a recent study performed at AECL Whiteshell Laboratories on the stability, structure and thermal load of non-vertical hydrogen-steam diffusion flames. The objective of this study is to determine the impact of a standing flame following a postulated nuclear reactor accident.

Experimental Facility

Experiments were performed in the Diffusion Flame Facility at Whiteshell Laboratories. A schematic of the facility is shown in Fig. 1. This facility consists of a 15-cm diameter burner mounted inside a modified grain silo (5 m in diameter, 8 m height). The burner and the silo were insulated such that experiments involving hydrogen-steam diffusion flames in air-steam environment can be performed. Various flame or jet structures can be created by mounting an orifice at the exit end of the burner. In this series of experiments, circular orifices with diameters of 6.35, 12.7 and 19.0 mm were used. The burner can also be tilted such that its orientation can be adjusted from horizontal to vertical. In this study, the burner was fixed at the horizontal position. Since experiments were performed in a confined space, the amount of hydrogen used in each experiment was limited to 2 m³ for safety reasons. To ensure ignition of the gas mixture, two propane pilot flames located 20 cm and 50 cm from the burner were employed. The steam composition in the jet was varied from 0 to 90% by volume in this series of experiments. The jet velocities were varied from 50 to 500 m/s. Within this range of experimental parameters, both the Reynolds numbers and the Froude numbers were much larger than 1000, indicating that the structure of the flame was momentum-dominated. Video cameras and arrays of thermocouples were used as diagnostic tools. An array of 1.5-mm Chromel-Alumel thermocouples (spaced 15 cm apart) was mounted along the centerline of the burner to monitor the flame length and the flame temperature. Each test was recorded on video to assess the stability of the flame. A flame was considered to be unstable when video records showed that the flame was not anchored between the exit of the burner and the first pilot flame.

Ignition and Stability of Diffusion Flames

Ignition and stability of diffusion flames are, in essence, similar phenomena. They are both controlled by the same mechanism. Ignition of a jet of hydrogen-steam mixture will occur if the mixture is within the flammability limits and the temperature, along the combustible interface between the fuel and the surrounding air is high enough to cause auto-ignition. If the hot combustion products can continuously ignite the emerging mixture, a stabilized flame results. It was observed that, for a fixed jet composition and break configuration (shape and size), a stable diffusion flame can only anchor itself in the vicinity of the break if the flow rate is less than a

certain critical value. Figure 2 shows the stability boundaries for three different orifice sizes. The experimental variables were steam composition in the jet and the jet velocity. It was observed that the destabilization of a diffusion flame occurs in two steps. These are referred to as lift-off and blow-out. As the velocity or the steam content of the jet is increased, the base of the flame (the start of the chemical reaction zone or the luminous zone) lifts off the burner and becomes fixed at some distance downstream. The distance between the burner and the base of the flame increases as the flow rate increases until the critical blow-off condition is reached. Beyond this critical condition, a flame cannot stabilize itself in the vicinity of the burner. For safety analysis, a lift-off flame is also regarded as a stable flame.

Results from this study show that the stability of the flame depends very strongly on the steam content in the jet, but is only mildly affected by the jet velocity. The effects on flame stability for a jet velocity being increased by one order of magnitude (from 50 to 500 m/s) are similar to an increase in steam concentration by 10 vol. %. Results also reveal that the stability is very sensitive to jet diameter. At a jet velocity of 200 m/s, the stability limit (expressed in terms of steam concentration in the jet) for orifice diameters of 6.35, 12.7 and 19.0 mm are 30, 30 and 70% respectively. This implies that diffusion flames are more stable for jets of larger diameters. Within the range of experimental conditions examined, no stable diffusion flame was observed for a hydrogen concentration in the jet less than 25% by volume. It should be pointed out that this limiting concentration depends on jet velocity. Shepherd [2] performed diffusion flame experiments with jet velocities lower than the range of velocities examined in the present series of experiments. He did not observe a stable diffusion flame for a hydrogen concentration less than 15% by volume. Depending on accident scenarios, the emerging fluid from a break in the primary heat transport system may have a hydrogen content higher than this limit value. If there is sufficient air in the atmosphere surrounding the break, a diffusion flame that anchors at the break is a possible outcome.

Flame Length

Classically, the flame length is defined as the distance between the burner and the point at which all of the jet fluid has mixed with the surrounding air and reacted [3]. Since most diffusion flames are unstable, flame lengths are difficult to measure accurately. Experimentally, they are obtained either by long-exposure photography or by temperature measurement along the centerline. In the former method, the length of the luminous region is taken as the flame length. A reference temperature needs to be defined in the latter method. Usually this reference temperature is chosen arbitrarily. Flame length, although not well defined, is of great significance to reactor safety analysis. Along the flame surface, the temperature can be conservatively assumed to be approximately equal to the constant pressure combustion temperature for stoichiometric hydrogen-steam-air mixtures. Depending on the steam content, this temperature can be as high as 2000°C.

For horizontal jet flames, buoyancy effects created by the hot combustion products can cause a flame to bend up. Depending on the momentum of the jet fluid, buoyancy can significantly reduce the flame length. To quantify the potential hazard of these flames, the penetration depth

is a more meaningful parameter to measure than the physical length of the flame. The penetration depth, L_p , for horizontal hydrogen-steam diffusion flames was measured for various jet steam concentrations and jet velocities. In this series of experiment, both the "photographic" method and the "reference temperature" method were used. Results are summarized in Fig. 3 and Fig. 4. To determine the scale effect on the penetration depth, three different sizes of jet diameters were examined. Figure 3 shows the penetration depth for dry hydrogen diffusion flames of various jet velocities. Results based on two different reference temperatures, 500 and 600°C, are compared to the results based on video records. In general, a higher reference temperature yielded a shorter penetration depth. Results also show that for a small diameter jet ($D_j = 6.35$ mm), the penetration depths determined using a video camera (solid circles) agreed well with those determined by temperature measurement (open circles). A reference temperature of 600°C was used. As the jet diameter increases, the two results differ significantly. For safety analysis, the temperature method would yield more meaningful results. Even though the penetration depth determined by the two methods are very different, they show similar trends. The penetration depth increases as the diameter of the jet increases. This trend agrees with the observation from an earlier experiment [1] on vertical diffusion flames. To examine the scale effects of hydrogen diffusion flames, the normalized penetration depths (L_p/D_j) for different orifices were compared with one another. For a jet velocity of 500 m/s (representing a choked flow at the orifice), the values of L_p/D_j (using a reference temperature of 600°C) for the 6.35, 12.7 and 19.0-mm diameter orifices were found to be 157, 95 and 79 respectively.

Many theories are available to predict flame lengths. Reviews of these theories can be found in reference [4] and [5]. For flames with high Froude numbers (momentum dominated), Hottel [6] and Dimptakis [7] found that the flame length can be expressed as the distance downstream at which a constant fraction of the entrained fluid has been mixed at the molecular level and has reacted with the original jet fluid. This observation seems to apply to all flames in which the reaction rates are sufficiently fast. Assuming that the entrainment rates for burning jets are the same as for non-reacting jets, flame lengths can be correlated as a function of jet parameters. It is found that the flame length (L) is linearly proportional to the product of the equivalence ratio, the square root of the density ratio and the jet exit diameter:

$$L = 10\phi \left(\frac{\rho_o}{\rho_\infty} \right)^{\frac{1}{2}} D_j$$

where ρ_o and ρ_∞ are the densities of the jet and ambient fluid respectively and D_j is the diameter of the jet. The equivalence ratio ϕ is defined as the mass ratio of air to jet fluid in a stoichiometric mixture. For example, a pure hydrogen jet fluid into air has a value of ϕ equal to 35. The ratio L/D_j is estimated to be roughly 90. This estimate agrees reasonably well with experimental observation on vertical hydrocarbon and hydrogen diffusion flames. This simple theory seems to be able to provide a reasonable estimate of the penetration depth for horizontal hydrogen-steam diffusion flames also.

Figure 4 shows the effects of steam in the jet on the penetration depth. Results from this figure were determined using the photographic method. Nevertheless, the effects of steam are clearly shown. Steam content in the jet has a strong effect in reducing the penetration depth of the

flame. Increasing steam content in the jet can render the flame unstable and lead to blow-off of the flame.

Flame Temperature

The primary threat from diffusion flames is the high thermal load imposed on nearby safety-related equipment. For a 1 kg/s hydrogen release rate, the diffusion flame can generate 120 MW of thermal power. A calculation of the rate of energy transfer between surfaces with surrounding hot gases is relatively straightforward if the temperature field is known. Knowing the thermal properties of the gas and the surface, the rate of energy transfer can be calculated using standard convective heat transfer equations available in reference texts [8,9]. The high thermal load usually results from direct impingement of the flame with the object [10]. Therefore, the most critical parameter in this calculation is the peak temperature in the flame. The peak flame temperatures were measured in the present series of experiments. Results are summarized in Fig. 5. For dry hydrogen diffusion flames ($D_j = 12.7$ and 19.0 mm), the peak temperatures along the centerline were measured to be about 1150°C . For small jet flames ($D_j = 6.35$ mm), the reaction zone is very localized and the thermocouple array in the facility does not have sufficient resolution to detect the peak flame temperature accurately. Nevertheless, the 1150°C is still much lower than the adiabatic constant pressure combustion temperature ($\sim 2000^\circ\text{C}$) of a stoichiometric hydrogen-air mixture. These results suggest that turbulent mixing with the surrounding cold air significantly reduces the temperature in the reaction zone. Furthermore, the peak temperature is found to be very sensitive to the mixture composition. The peak temperature decreases as the steam content in the jet increases. However, for high velocity jets, the peak temperature is not very sensitive to the variation in jet velocity.

In most nuclear reactor accident scenarios involving a break in the primary heat transport system, the emerging fluid will contain some steam. Based on the results from this study, the peak temperature is expected to be lower than 1000°C . This temperature is lower than the melting temperature of most metallic surfaces except aluminum. This implies that the safety equipment inside the containment building can be easily protected by shielding with sheet metal. It should be pointed out that this temperature is still higher than the melting temperature of most cable insulation. These results have implications in hydrogen management following a nuclear reactor accident. The threat of hydrogen-steam diffusion flames created by the deliberate ignition scheme requires consideration but may not be as severe as anticipated by the conservative, adiabatic constant pressure combustion assumption, using stoichiometric mixtures.

Conclusion

A systematic study on the stability and the structure of various horizontal hydrogen-steam diffusion flames was performed in the Diffusion Flame Facility at AECL Whiteshell Laboratories. Jet conditions similar to those of emerging fluid at a break in the primary heat transport system after a nuclear reactor accident were examined. It was observed that, for a certain range of hydrogen-steam compositions, a stable diffusion flame can exist and anchor itself at the break. Due to the presence of steam and turbulent mixing with the surrounding cold

air, the peak flame temperature is much lower than the adiabatic constant pressure combustion temperature of a stoichiometric hydrogen-air mixture. These results suggest that the safety equipment inside the containment building can be protected by shielding with sheet metal. As a result, the threat of a diffusion flame anchored at the break may not be as severe as anticipated. Furthermore, such a flame can remove hydrogen at the source and significantly reduce the possibility of a global gas explosion.

Acknowledgment

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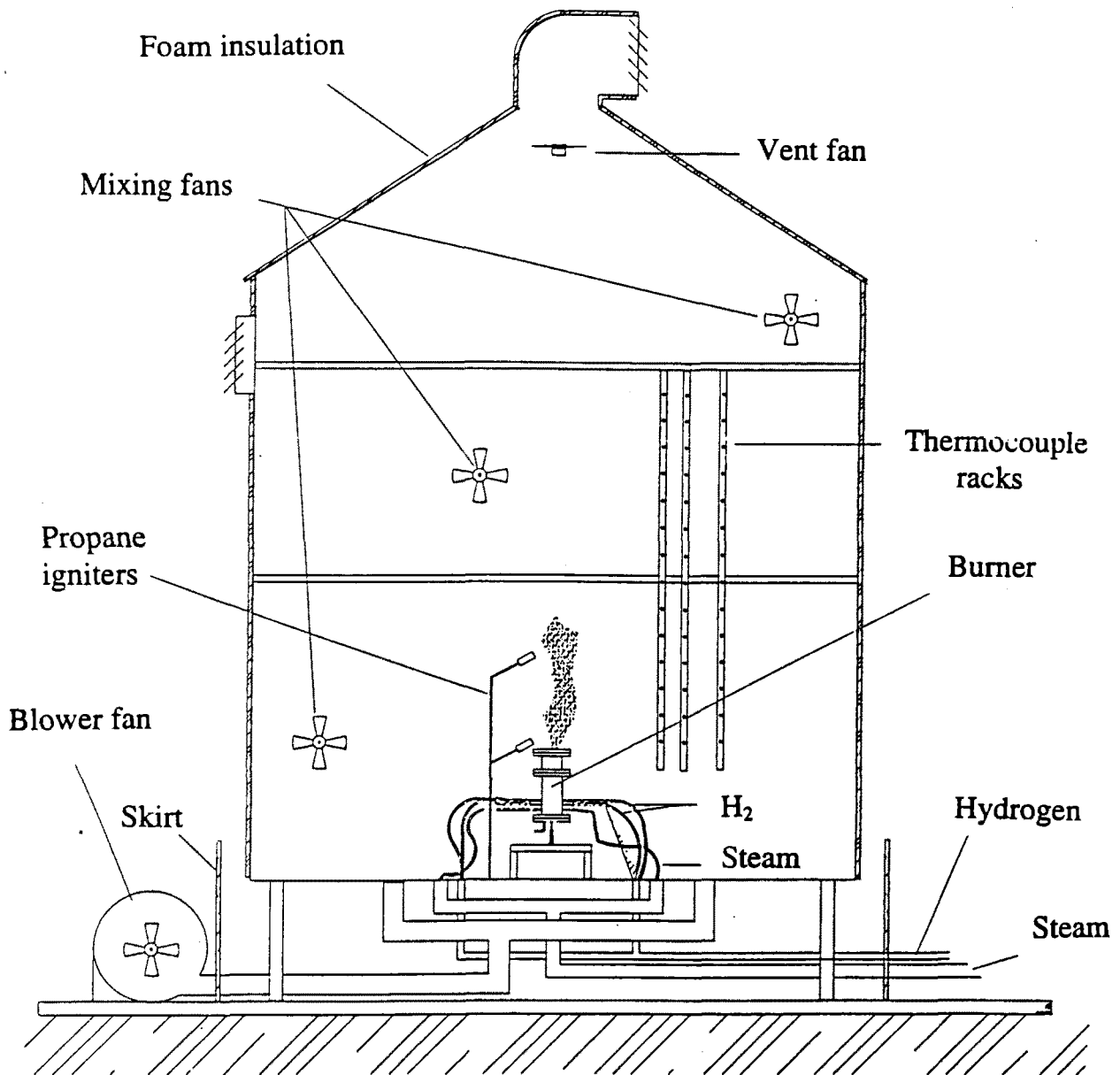


Figure 1. Schematic of the Diffusion Flame Facility.

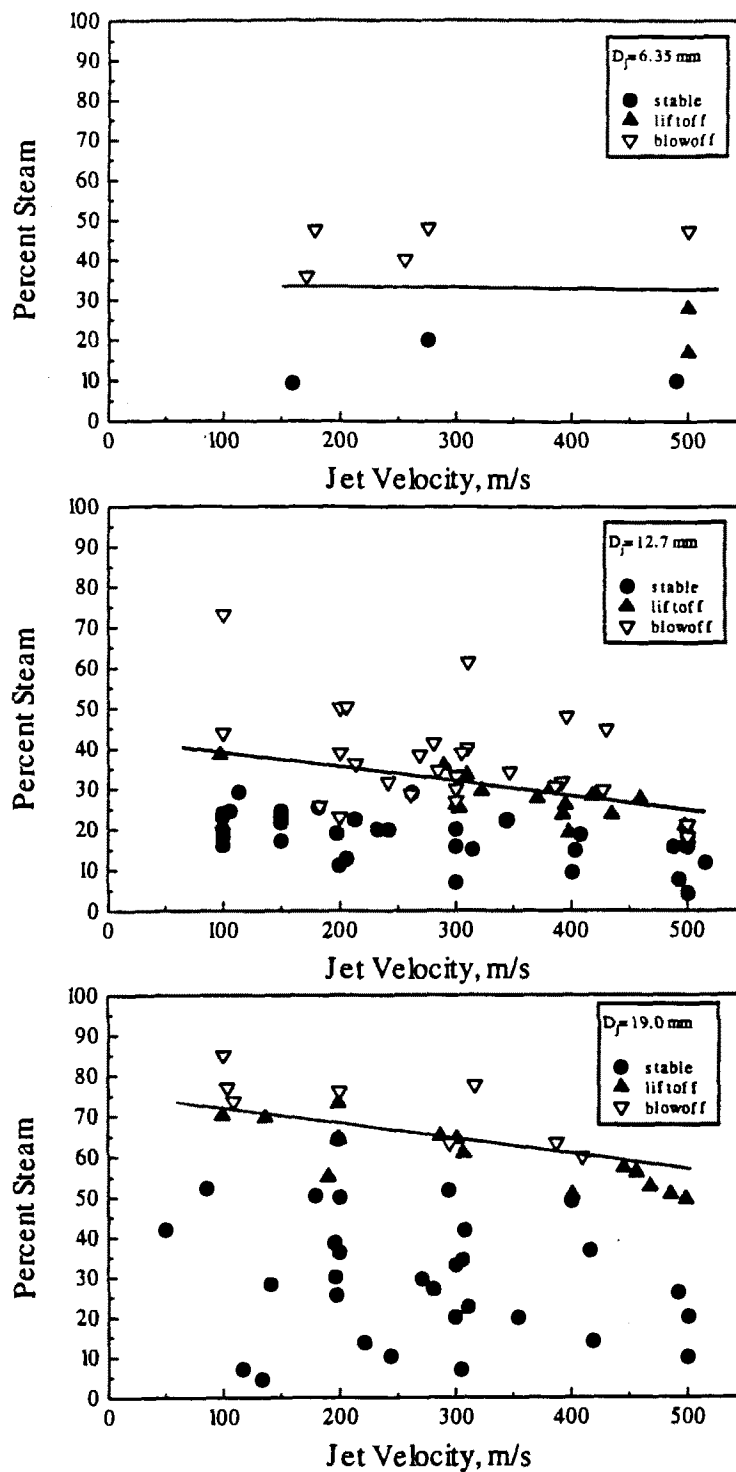


Figure 2. Stability Regimes for Various H_2 -Steam Diffusion Flames
($50 \text{ m/s} < V_j < 500 \text{ m/s}$, $D_j = 6.35 \text{ mm}$, 12.7 mm and 19.0 mm).

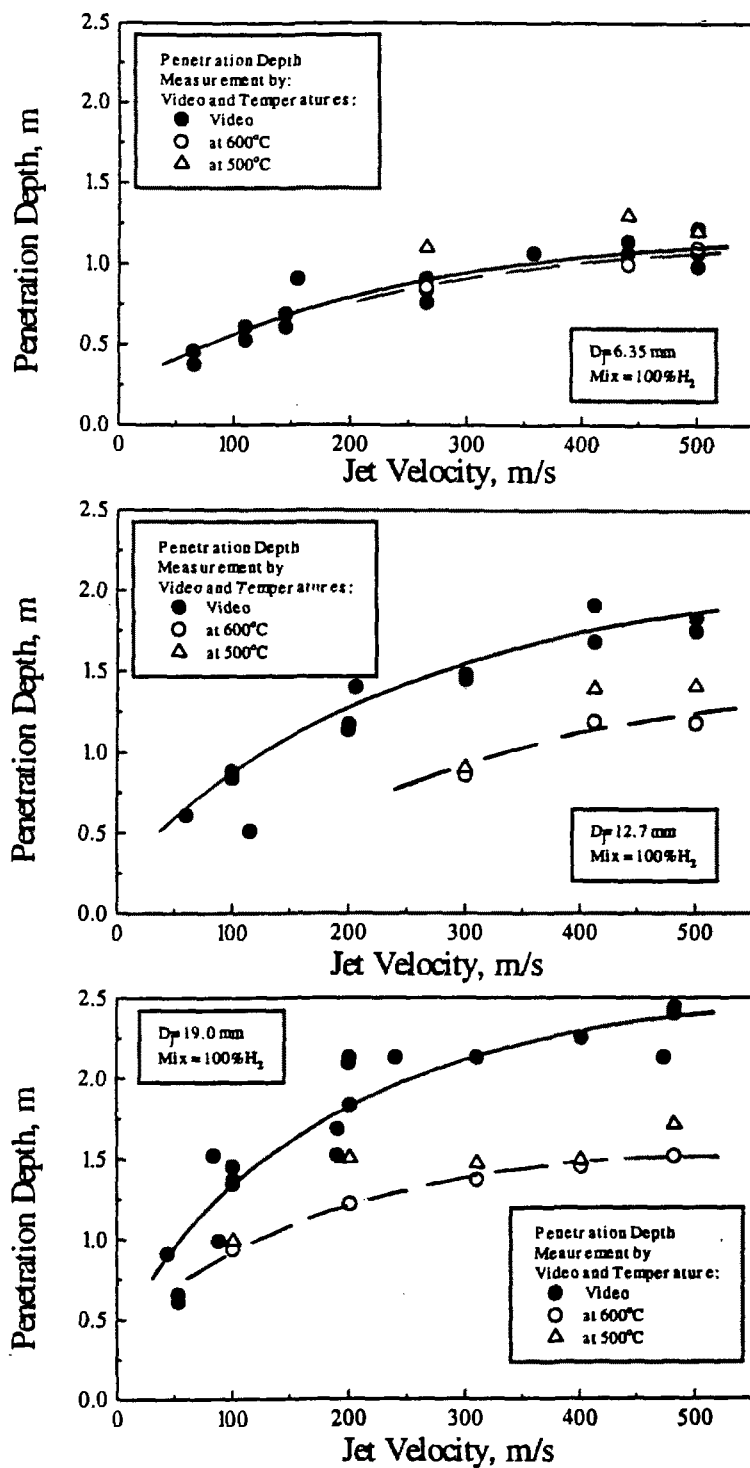


Figure 3. Penetration Depths of Various Dry H₂ Diffusion Flames
(50 m/s < V_j < 500 m/s, D_j = 6.35 mm, 12.7 mm and 19.0 mm).

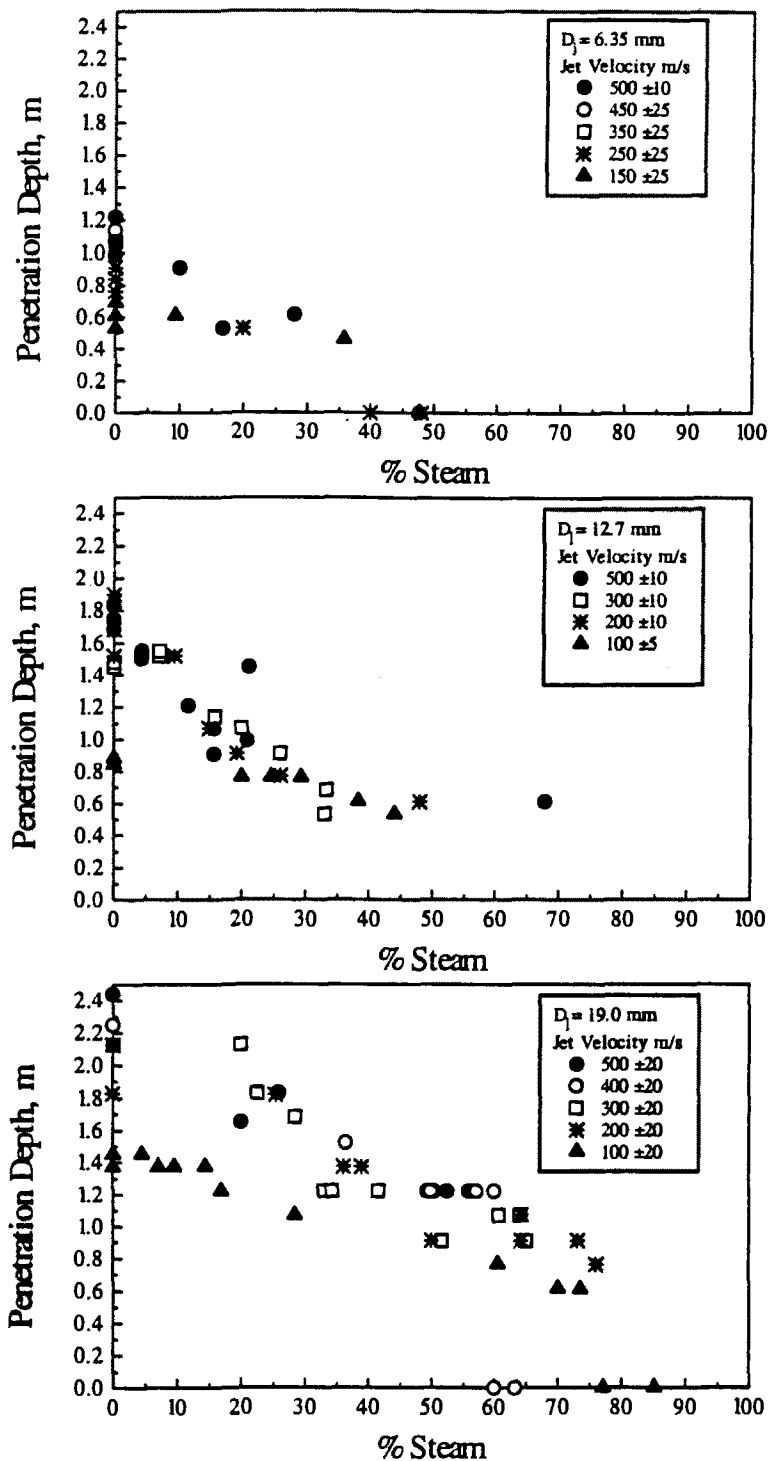


Figure 4. Penetration Depths of Various H_2 -Steam Diffusion Flames.

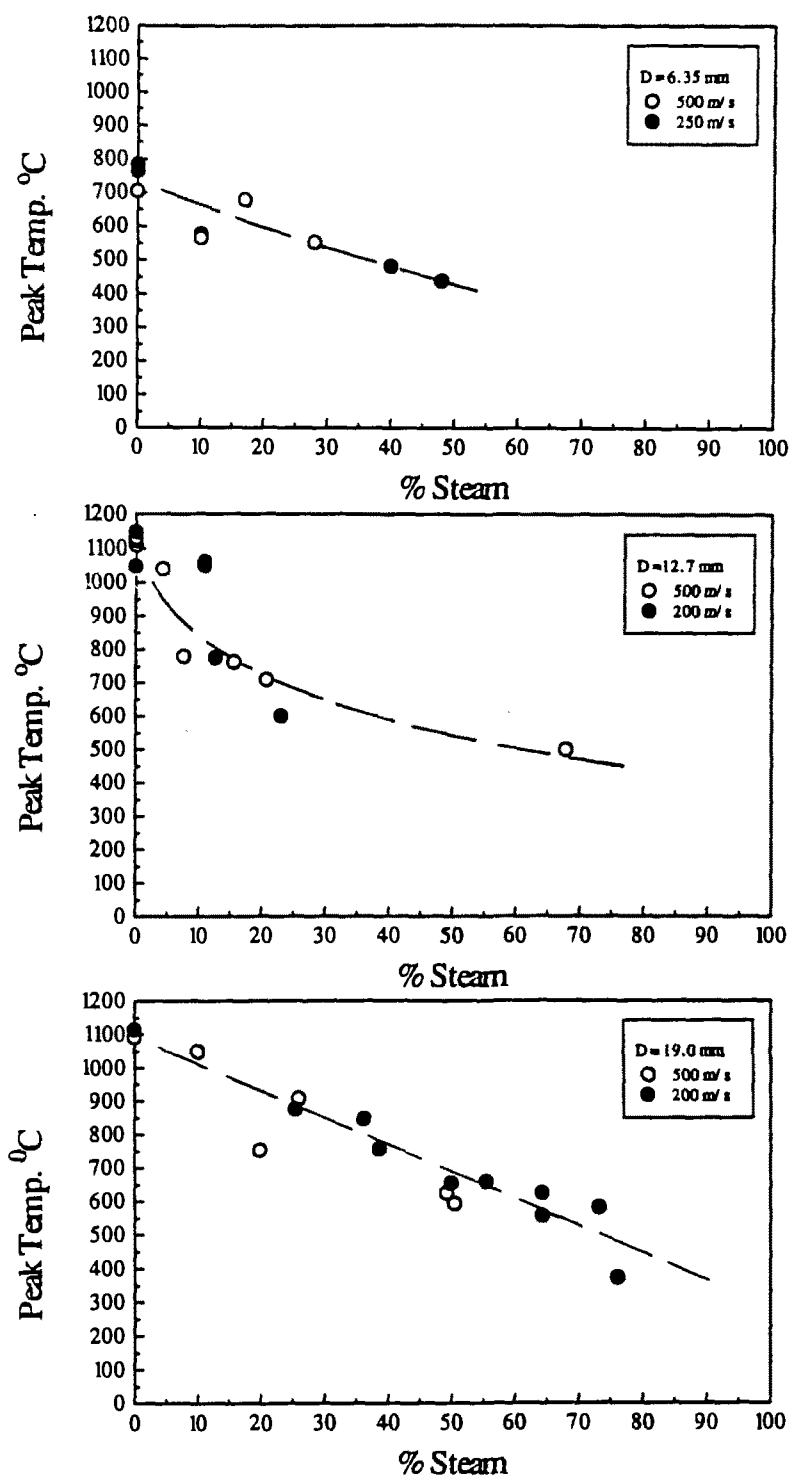


Figure 5. Peak Flame Temperatures of Various H₂-Steam Diffusion Flames.

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5.5 MODELLING OF HYDROGEN DEFLAGRATION IN VESSELS USING GOTHIC

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Abstract

Simulations of hydrogen deflagration tests were performed using the discrete lumped-parameter burn model of the computer code GOTHIC. The tests were performed in small and large scale spherical vessels and a cylindrical vessel. The small vessel cases included the effects of venting, and the cylindrical tests included the effects of obstacles. The simulations were performed by sub-dividing the volumes into either five or ten "cells", and parameters such as flame speed and hydrogen concentration were varied. Measured flame speeds were used in the simulations and the results were compared to simulations using the code "default" flame speed.

The calculated pressure transients compared well with the experimental results using the measured flame speeds in the simulations of unvented cases, whereas for vented cases, the predicted peak pressures were generally less than the measurements. However, when the code default flame speed is used, the predicted peak pressures were more consistent and generally conservative when compared with the measurements.

When the default flame speeds were used for vessels without obstacles, the peak pressures obtained were higher and the burn times were shorter than the experimental measurements. This was probably due to the basis for the correlations used for default flame speed in the burn model. These correlations were derived from intermediate-scale experiments for hydrogen combustion in relatively turbulent (fans on) environments. For vessels without obstacles, laminar flame speeds were more likely. Hence, the predicted peak pressures would be expected to be higher than the experimental results.

In order to account for the degree of turbulence and flame acceleration caused by the presence of obstacles, higher than default flame speeds were used in the simulation of the vessel with obstacles. It was found that twice the default flame speed provided predictions of

peak pressures comparable to the measurements.

Based on the simulations conducted, the GOTHIC lumped-parameter burn model adequately predicts the peak pressure due to hydrogen deflagration in containment.

Introduction

As part of code validation efforts, the GOTHIC [1,2] computer code was used to simulate hydrogen deflagration in a series of experiments. Four sets of experiments were chosen as test cases [3,4,5,6] because of the simple geometry of the facilities (classical spherical/cylindrical shapes) and the completeness of the data.

This paper consists of a brief description of the test facilities, the experimental set-up and the results. The simulation methods and assumptions are described. The calculated results are summarized and compared with the experimental results. The comparison is discussed and conclusions are drawn.

Background

During a severe Loss of Coolant Accident (LOCA) coincident with failure of emergency cooling, hydrogen can be produced in significant quantities from in-core metal-water reaction at the zircaloy fuel sheath surfaces. When this hydrogen is released into containment through the pipe break, there arises the potential for a burn. If sufficient hydrogen is released, the burn may produce peak pressures that challenge the integrity of containment. To protect containment structural integrity, a Hydrogen Ignition System is installed in Ontario Hydro reactors to deliberately burn hydrogen near the lower flammability limit, before more sensitive mixtures can form. To assess the effectiveness of this installed hydrogen mitigation and control strategy, methods are needed to simulate the peak pressure in containment during a hydrogen deflagration. The computer code GOTHIC, and its lumped-parameter burn model, appears to be well suited for such calculations, and work is reported here which forms part of code validation.

Brief Description of the Test Facilities Being Simulated

Small Scale (2.3 m) Sphere - Closed and Vented

Figure 1 is a picture of the experimental test facility presented in Reference 3 (the closed

case) and Figure 2 is a schematic of the experimental set-up used in Reference 4 (the vented case). Both experiments used a 2.3-m diameter sphere. In the closed configuration (experiment described in [3]), there was no vent whereas for the vented configuration (experiment described in [4]), the sphere was connected to a 45-cm internal diameter, 3-m long duct open to atmosphere. The open end of the pipe could accommodate rupture disc of 15, 25 or 45-cm. An air-motor driven fan was installed inside the sphere to ensure the hydrogen was well-mixed initially.

Large Scale (16 m) Sphere

Figure 3 shows the US Department of Energy's Nevada Test Site (NTS) test vessel for large-scale combustion tests [5]. This NTS facility had an inside diameter of 16 m and a volume of 2100 m³. It was nearly two orders of magnitude larger (by volume) than those previously used in hydrogen combustion research.

Cylindrical Vessel

Figure 4 shows schematic diagrams of various vessel configurations [6]. The apparatus used in these experiments, was a cylindrical vessel 4.27 m in length and 1.22 m in diameter. It had a semi-ellipsoidal closed end and a flange at the exit, to which a flanged semi-ellipsoidal cover of depth 0.61 m could be attached. The total length of the vessel with the cover in place (closed mode) was 4.88 m. However, when the cover was replaced by a polyethylene bag (vented mode), the region containing combustible gas was longer.

Annular ring baffles made of 1.27 cm thick plywood were used in some tests to provide obstacles for inducing turbulence and flame acceleration. The outer diameter was cut to fit the inner diameter of the vessel. The inner diameter was 0.86 m, which gave an areal blockage ratio of 0.5. Angle irons attached to the vessel wall were used to clamp the baffles in position.

The GOTHIC Code

The burn model in the GOTHIC computer code [1,2] version 4.1, is used in the study of peak pressure rise due to hydrogen deflagration. This code has incorporated the burn models from HECTR (Hydrogen Event Containment Transient Response) [7] which was developed at Sandia National Laboratories, Albuquerque (SNLA), for modelling the transportation and combustion of combustible gases during postulated accidents in LWRs.

HECTR [7] is a multi-compartment lumped-parameter code relying on conservation correlations and simple phenomenological and empirical models. The combustion models consist of correlations for ignition, propagation between compartments, combustion

completeness, and flame speed. HECTR does not model a flame front, but calculates the duration of a burn (burn time) and the final mole fractions at the start of a burn. It also calculates a uniform (non-expanding) volumetric burning rate such that the burn finishes at the predetermined time with the correct final mole fractions. The combustion rate is adjusted each time step to account for sources and intercompartment flows and to complete the burn at the originally specified time.

Method Used in the Simulation of the Experiments

Modelling

The closed sphere was modelled by subdividing it into either five or ten volumes (cells). The first (centre) volume was spherical and the others were spherical shells propagating from the centre volume towards to the outer surface. Five or ten volumes were shown to be sufficient in an earlier study on spatial convergency [8]. The hydrogen burn originated from the centre and propagated to the outer volumes. Similarly, in simulating the cylindrical vessel, the volume of the cylinder was treated as a spherical volume which was modelled by dividing into five cells.

For the small, vented spherical vessel, the outermost volume of the sphere was connected to the 3-m long pipe which was represented as another volume. The end of this pipe was then vented to the atmosphere. Venting to atmosphere was modelled as a valve connection to an infinite volume at atmospheric conditions.

Input Data/Assumptions

1. All simulations were performed using input data of temperature, pressure and relative humidity as specified in the published papers.
2. Only centre ignition in the sphere was simulated.
3. The burn time fraction was arbitrarily set at 0.95, that is, each burn was assumed to start propagating to the adjacent cell at 95% of the burn duration. It is not practical to assume a burn fraction of 1, since that would imply the flame front travels in a sharp and ideal plane during the propagation.
4. The radius of the sphere was equally divided into either five or ten different number of cells. That is, each cell of the sphere had the same burn length but different volume.
5. The flow area of each cell was the surface area of each sphere, since the burn propagated in all directions from the surface of the inner sphere.
6. The loss coefficient at the junction between each cell of the spherical volume was arbitrarily set at 0.001 (i.e. minimal loss at each junction). For the vented spherical vessel, the loss coefficient at the junction between the sphere and the pipe was calculated to be 0.5. The loss coefficient at the exit to the atmosphere was calculated

to be 2.77 for 15 cm and 25 cm vent sizes and 1.23 for 45 cm vent size. For the vented, cylindrical vessel, the loss coefficient at the exit to atmosphere was calculated to be 1.0.

7. For all the cases simulated, the combustion was assumed to be complete.
8. Measured flame speeds, the code default flame speed and twice the default speed were used in the simulations.

Results

Tables 1 and 2 show the peak pressure due to hydrogen deflagration in small- and large-scale vessels, respectively. The results show the peak pressure increases with hydrogen concentration. When default flame speeds are used, the burn times are less than the experimental measurements while the peak pressures remain the same. With the same hydrogen concentration (7.8% H_2), as in the large-scale vessel, a higher percentage of steam in the gas mixture results in a slightly lower peak pressure.

Table 1 shows the pressure transients and the predicted peak pressures with various vent sizes. The results show the peak pressure decreases with larger vent sizes. When hydrogen concentration increases, the burn time decreases. Simulation using the measured burn time predicts a slightly higher peak pressure than measured for the case with 15 cm vent size.

Table 1 also shows the pressure transients and the predicted peak pressures using default flame speed and twice the default flame speed. When the default flame speed is used in the simulation, the predicted peak pressure is higher and occurs in a shorter time. When twice the default flame speed is used, the predicted peak pressure rise is slightly higher than when default flame speed is used (see Figures 7 and 8). Pressure transients simulated by using high burn rate or large vent sizes show some oscillations and instabilities.

Table 3 shows the predicted peak pressure for the cylindrical vessel. When default flame speeds are used, the burn times are less than measured but the peak pressures remain about the same in all closed cases. As the hydrogen concentration increases, the peak pressure also increases. For both vented with and without obstacles cases, the pressure transient curves show oscillations and instabilities. When the default flame speed is used for cases with obstacles, the peak pressure is slightly less than the measured peak pressure. However, when twice the default flame speed is used, the predicted peak pressure is comparable to the measured peak pressure.

Discussion

Pressure Transient

For both closed and vented cases, the pressure transient curves show a similar behaviour (see Figures 5 and 6). The smaller the volume of hydrogen being burnt, the lesser the pressure build-up initially. Since the overall radius is divided equally for each cell, the volume of hydrogen increases geometrically as it approaches the last few cells. As the burn length remains the same for each cell, the burn rate is higher for the last few cells. This results in a rapid pressure rise toward the later stage of the burn and hence the final peak pressure is also higher.

When hydrogen concentration increases, the burn time decreases. Hence, the flame speed increases and the rate of pressure rise increases. More hydrogen is burnt in a shorter period of time resulting in a higher peak pressure.

For closed vessels, the predicted peak pressures for various hydrogen concentrations are consistently higher than the measured pressures. Although the peak pressure for deflagration in a closed volume is primarily a function of the total energy released and the extent to which heat is lost to the surroundings, there are always some unburnt hydrogen residues expected to remain in the vessels. Due to the assumption that all hydrogen is burnt completely in the simulations and no pressure losses due to instrumental leakages, the calculated peak pressures by the GOTHIC burn model are anticipated to be higher than the experimental measurements.

For both closed and vented cases, the predicted peak pressures are higher and occur in a shorter time when default flame speeds are used instead of measured flame speeds. This is mainly due to the flame speed correlations used in the burn model. The flame speed correlations for hydrogen combustion are based on intermediate-scale experiments that were performed in the Variable Geometry Experimental System (VGES) and Fully Instrumented Test (FITS) [9,10] experimental facilities at SNLA and Nevada Test Site (NTS) [11]. These experiments were carried out in relatively turbulent (fans on) environments while laminar environments are expected in small-scale experiments. Hence, the resulting peak pressures estimated by using the default flame speed are expected to be higher than the experiments due to the turbulence effects.

The peak pressure for closed vessels remains about the same for all default flame speeds used. The pressure rise for deflagration in a closed volume is primarily a function of the total energy released and the extent to which heat is lost to the surroundings. The total energy released during the burn is a function of mixture composition and stoichiometry whereas heat losses to the surroundings depend on enclosure size and geometry. Therefore, pressure rise is independent of the mixture burning rate (i.e. the flame speed).

For vented without obstacles cases, the peak pressures decrease with larger vent sizes. It is obvious that when the size of vent is larger, more hydrogen and flue gases are vented to the atmosphere and hence the peak pressure is less. Because both mass and energy are lost from the vessel during the burn, the peak pressure is a function of the balance between the rate of

burnt gas production and the rate of venting; the rate of gas venting is a function of gas density, vent area and pressure drop.

As the vent size increases, the predicted peak pressures using measured flame speed are less than the measurements. In the measurements, there are fluctuations and instabilities shown in the pressure transients (see Figure 5) as the vent sizes increases. As stated in the paper [3], severe oscillations were superimposed on the pressure transients measured. The amplitude of such pressure oscillations was large and of the order of the mean pressure itself. Therefore, there are uncertainties in the peak pressure measurements.

However, the predicted peak pressure increases when default flame speed is used. This is probably due to the higher flame speed and the shorter burn time which allow less time for gas venting. The burn completes in such a short time that the pressure rises rapidly and venting effect becomes less important. Nevertheless, GOTHIC simulations using default flame speed consistently reflect conservatism over the experimental measurements and the pressure transient curves compare very well with the experimental results.

When obstacles are present, the peak pressure as well as the burn time can be affected. Obstacles placed in the path of a propagating flame front are known to increase the mixture burning rate through local flame acceleration effects. Tests show that obstacle-induced flame acceleration is a strong function of obstacle spacing, blockage ratio and shape [12,13]. Also, it is well known that large scale fluid eddies increase the effective flame area due to flame folding effects and that finer scale eddies increase the effective flame area and the mixture burning velocity due to enhanced rates of heat and mass transfer [14,15]. As a result, the rate of burnt gas production generally increases with finer scales of turbulence and increasing turbulent intensities.

In GOTHIC simulations, the default flame speed can be overwritten by a specified flame speed. In order to derive the flame speed due to flame acceleration caused by the turbulence generated by obstacles, different flame speeds are used and the peak pressures are compared with the experimental measurements. It is noticed that twice the default flame speed appears to be sufficient in providing comparable estimations for vented with obstacles vessel with an areal blockage ratio of 0.5 (In a reactor containment, it is very unlikely to have an areal blockage ratio (due to equipment) of greater than 0.5).

Flame speed affects both combustion pressure and temperature, because it controls the time variable for heat transfer during the burn and for venting into other cells. Burns that are less than a few seconds in duration (with a fast burning rate) become nearly adiabatic (little time for heat transfer or loss to the surroundings). Hence, the effect of heat transfer on the peak pressures has not been considered and GOTHIC simulations are expected to provide a marginally higher peak pressure than measurements.

Burn Time

The burn times using the default flame speeds for vessels in the absence of obstacles, are shorter than the measured burn time. This has been explained earlier, the default flame speed

in GOTHIC is expected to be higher than the actual flame speed due to the turbulence effects embedded in the flame speed correlations. When twice the default flame speed is used, the burn time is about half the burn time for default flame speed (see Figures 7 and 8).

Comparing Figures 9 and 10, the pressure transient curve from GOTHIC shows that the peak pressure occurs in a shorter burn time than the measurements when obstacles are present. Since the default flame speed in GOTHIC is higher than the actual flame speed (as mentioned earlier) during the initial period of the burn, it is expected to predict a faster burn than the measurements and complete the burn in a shorter time than measured. However, in the experiment, deflagration starts in a laminar environment until it reaches the first obstacle (baffle). The baffles in the combustion vessel enhance the development of large-scale fluid eddies and vortical motion, intensify the turbulence level, and accelerate the flame in the flowfield. As the flame propagates and accelerates, (from left to right, see Figure 4), through the baffles inside the vessel, the burn rate increases abruptly which causes the peak pressures to rise rapidly in a very short time towards the end of the burn. Therefore, the pressure transient curve (see Figure 9) shows minimal pressure increase initially and rises to the peak pressure in an extremely short period and completes the burn. The GOTHIC lumped-parameter burn model has a limitation in simulating this gradually developing turbulence effect and flame acceleration phenomenon. Though the model predicts a shorter burn time, it predicts the peak pressure comparable to the experimental measurements.

Conclusions

In all the cases modelled for both closed and vented, with and/or without obstacles, the results from the simulations using the GOTHIC lumped-parameter burn model reflect consistently conservative estimations of the peak pressures when compared with the experimental results. The comparisons have demonstrated that the GOTHIC burn model can be used in predicting the peak pressure due to hydrogen deflagration in containment.

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TEST DATA				GOTHIC-HECTR		
%H2	%AIR	BURN DURATION (sec)	PRESSURE RISE(kPa)	PRESSURE RISE (BURN DURATION) (kPa & sec)		
				Sim.	DF1	DF2
CLOSED VESSEL						
10	90	0.87	215	247	247(0.16)	251(0.08)
15	85	0.11	303	347	349(0.08)	346(0.04)
20	80	0.14	389	431	437(0.05)	433(0.03)
VENTED VESSEL						
(a)	15 cm diameter vent					
10.0	90.0	1.060	159	181	269(0.18)	283(0.10)
12.4	87.6	0.440	291	282	328(0.14)	340(0.08)
15.0	85.0	0.277	364	349	385(0.11)	401(0.06)
20.5	79.5	0.130	470	477	493(0.09)	532(0.06)
(b)	25 cm diameter vent					
10.0	90.0	1.040	93	70	235(0.17)	268(0.09)
14.8	85.2	0.299	295	273	356(0.10)	395(0.06)
20.0	80.0	0.120	453	428	464(0.07)	482(0.04)
(c)	45 cm diameter vent					
10.0	90.0	1.100	30	8	118(0.17)	198(0.09)
15.0	85.0	0.256	214	98	228(0.09)	258(0.06)
15.9	84.1	0.226	218	122	204(0.10)	283(0.06)
19.7	80.3	0.120	327	273	330(0.06)	420(0.03)
20.3	79.7	0.109	394	289	441(0.06)	538(0.03)

Sim = Simulation using measured burn time
DF1 = Simulation using default flame speed
DF2 = Simulation using twice the default flame speed

Table 1 Predictions of Peak Pressures for Small-Scale Vessels

TEST DATA					GOTHIC-HECTR	
%H2	%AIR	%STM	BURN DURATION (sec)	PRESSURE RISE(kPa)	PRESSURE RISE(BURN DURATION) (kPa & sec) Sim. DF1	
7.7	87.5	4.8	9.4	219	251	254(1.7)
7.8	87.8	4.4	9.2	214	251	255(1.7)
7.8	60.9	31.3	15.0	149	180	183(12.8)
8.1	53.2	38.7	20.0	110	187	186(25.2)
9.9	85.9	4.2	4.9	278	327	329(1.4)
10.1	60.4	29.5	6.6	223	273	271(8.9)
12.9	59.3	27.8	3.4	303	350	350(5.2)
13.2	59.4	27.4	2.0	300	351	347(4.8)

Table 2 Predictions of Peak Pressures for Closed Large-scale Vessel

TEST DATA				GOTHIC-HECTR		
%H2	%AIR	BURN DURATION (sec)	PRESSURE RISE(kPa)	PRESSURE RISE(BURN DURATION) (kPa & sec)		
				Sim.	DF1	DF2
Closed Volume						
15	85	0.75	310	374	373(0.08)	378(0.04)
30	70	0.075	490	615	614(0.04)	615(0.02)
Vented						
15	85	0.24	4	7.6	13(0.07)	62(0.04)*
30	70	0.045	45	96	88(0.04)	260(0.02)*
Vented with Obstacles						
15	85	0.23	120	8	31(0.08)	113(0.04)**
30	70	0.045	370	179	173(0.032)	369(0.019)**

- * The peak pressure rises for 15% and 30% fluctuate between 37-86 kPa and 222-296 kPa, respectively.
 ** The peak pressure rises for 15% and 30% fluctuate between 94-132 kPa and 345-393 kPa, respectively.

Table 3 Predictions of Peak Pressures for Cylindrical Vessel

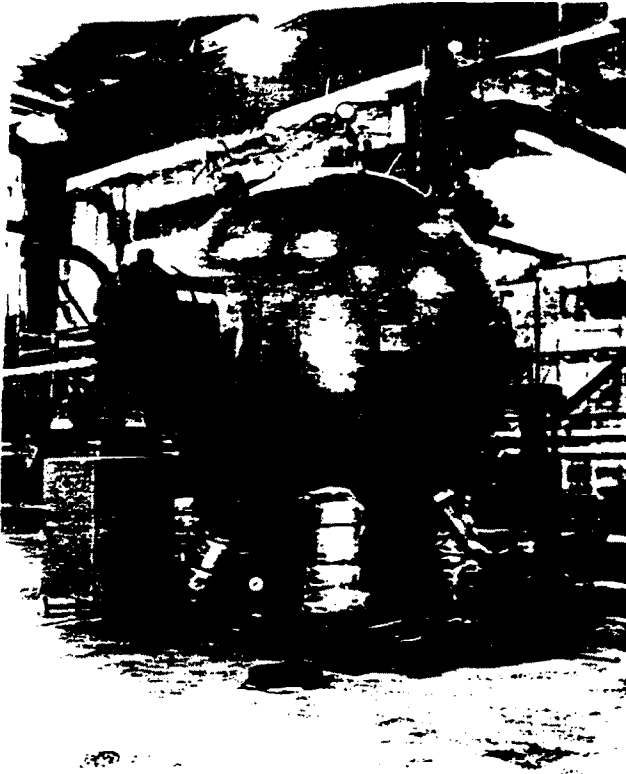


Figure 1 A View of the Sphere
(Ref.3)

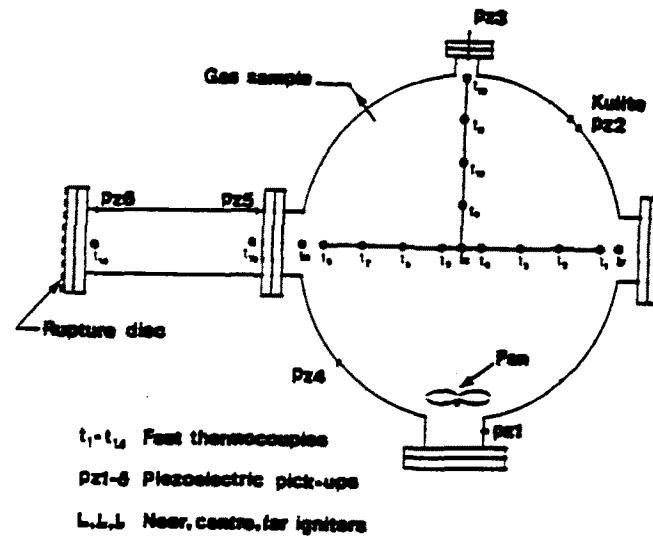


Figure 2 Schematic of the Vented Vessel
(Ref.4)

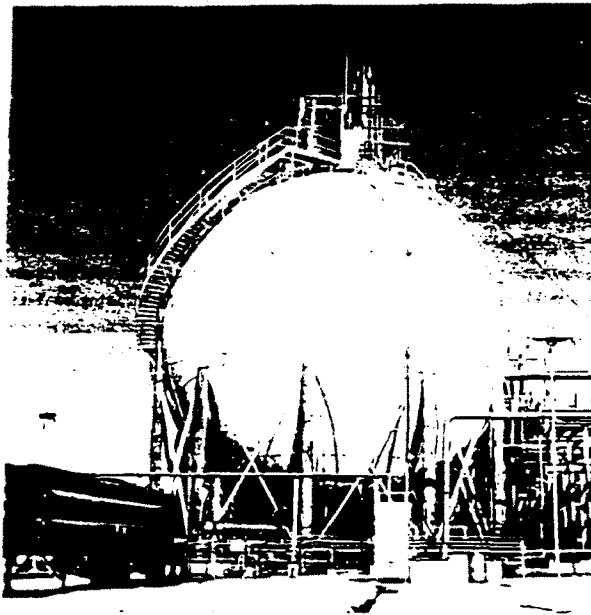


Figure 3 A View of the Large-Scale Test Vessel
(Ref.5)

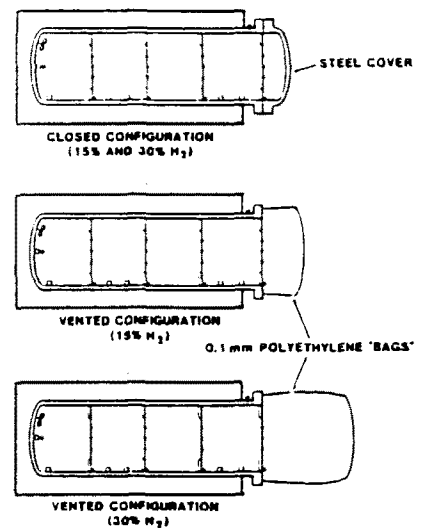


Figure 4 Schematic Diagrams of the Cylindrical Vessel
(Ref.6)

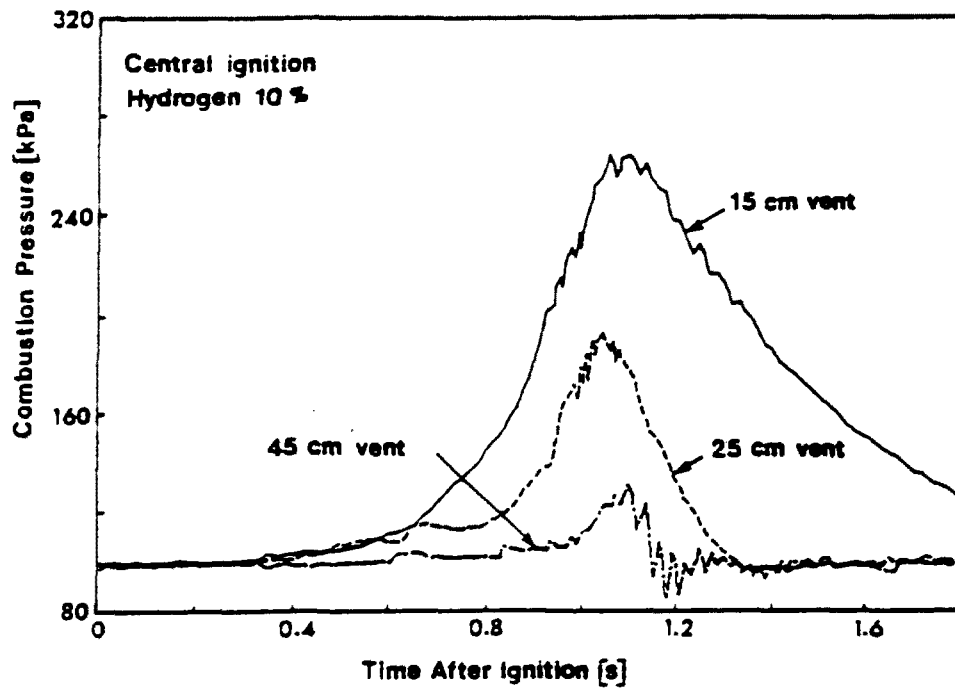


Figure 5 Pressure Transients at 10% Hydrogen for Various Vent Sizes [Ref. 4]

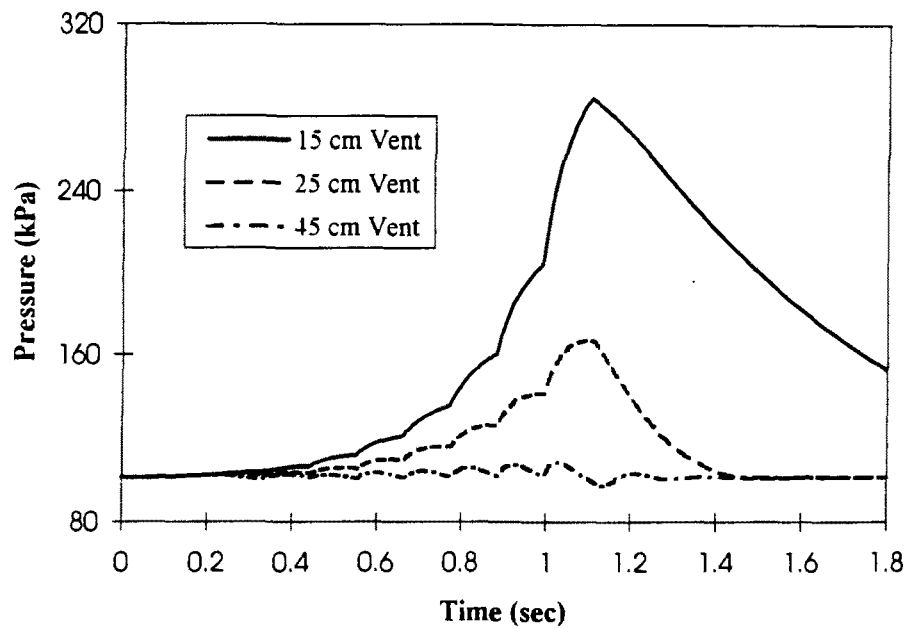


Figure 6 Predicted Pressure Transients Using Different Vent Sizes for the Vented Vessel, 10% Hydrogen

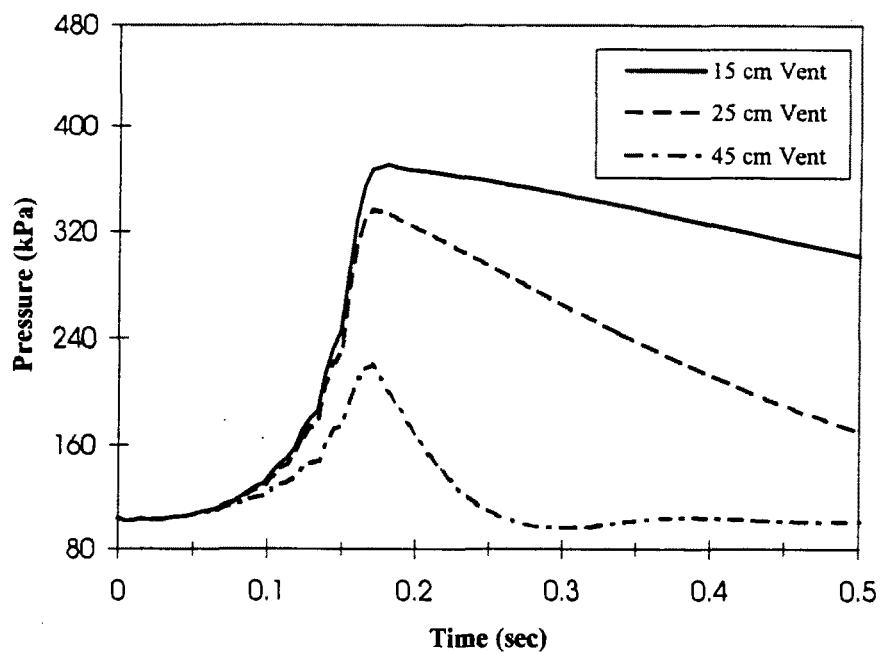


Figure 7 Pressure Transients Using Default Flame Speed for the Vented Vessel, 10% Hydrogen

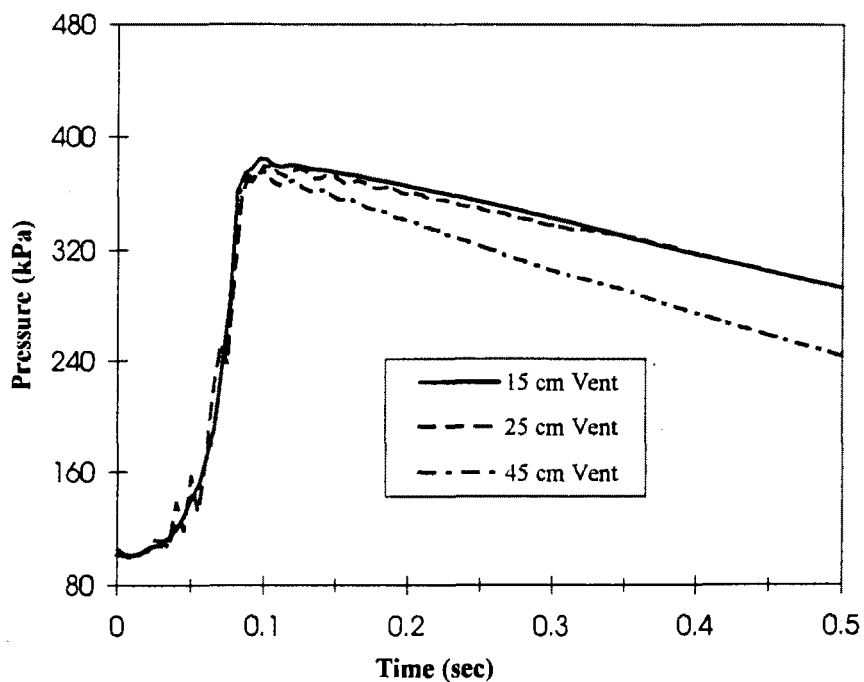


Figure 8 Pressure Transients Using Twice the Default Flame Speed for the Vented Vessel, 10% Hydrogen

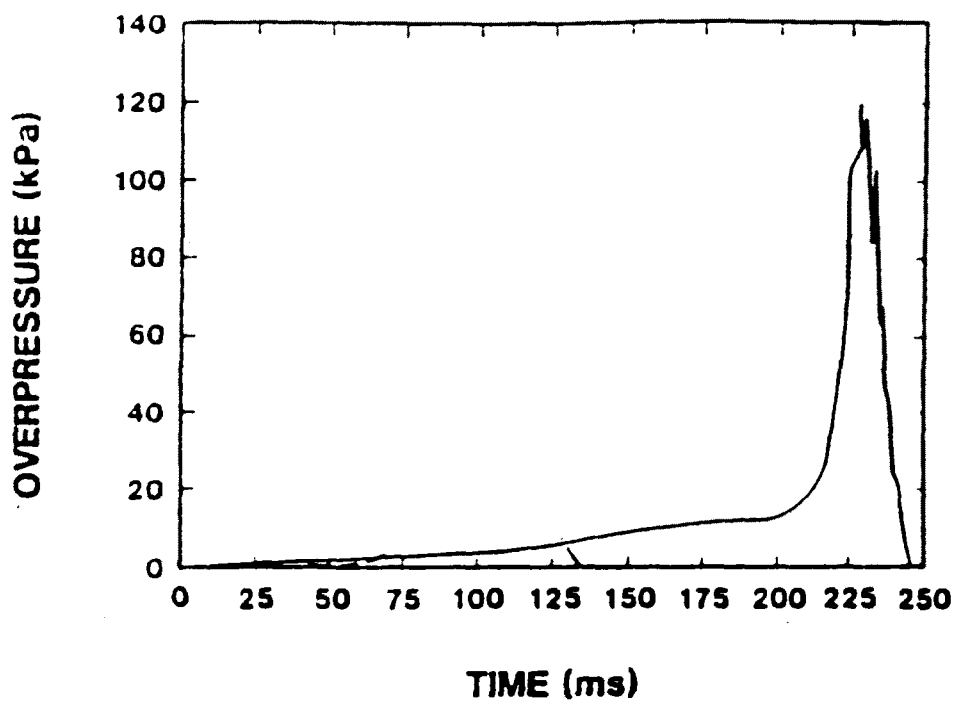


Figure 9 Combustion Overpressure Data for Vented with Obstacles Case [Ref. 6]

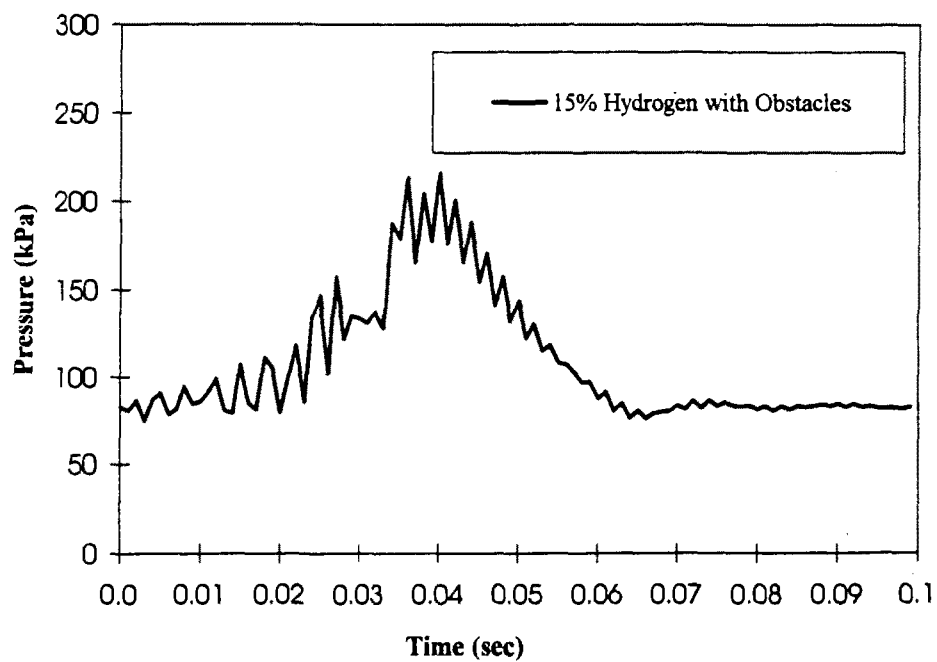


Figure 10 Pressure Transient Using Twice the Default Flame Speed for Vented with Obstacles Case

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SESSION VI

NEW DESIGNS

Chairman: B. De Boeck (AVN, Belgium)

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6.1 CONTAINMENT AIR CIRCULATION FOR OPTIMAL HYDROGEN RECOMBINATION

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Abstract

An accepted first-line defense for hydrogen mitigation is to design for the hydrogen to be rapidly mixed with the containment atmosphere and diluted to below flammability concentrations. Then, as hydrogen continues to be produced in the longer term, recombiners can be used to remove hydrogen: recombiners can be located in forced-air ducts or passive recombiners can be distributed within containment and the heat of recombination used to promote local air circulation. However, this principle does not eliminate the possibility of high hydrogen concentrations at locations removed from the recombiners.

An improvement on this strategy is to arrange for a specific, buoyancy-driven, overall circulation of the containment atmosphere such that the recombiners can be located within the recirculation flow, immediately downstream of the hydrogen source. This would make the mixing process more predictable and solve the mass-transfer problem associated with distributed recombiners. Ideally, the recombiners would be located just above the hydrogen source so that the heat of recombination would assist the overall circulation. In this way, the hydrogen would be removed as close as possible to the source, thereby minimizing the amount of hydrogen immediately downstream of the source and reducing the hydrogen concentration to acceptable levels at other locations.

Such a strategy requires the containment volume to be divided into an upflow path, past the hydrogen source and the recombiner, and a downflow path to complete the circuit. The flow could be generated actively using fans or passively using buoyancy forces arising from the difference in density of gases in the upflow and downflow paths; the gases in the downflow path being cooled at an elevated heat sink.

Introduction

The conventional principle for hydrogen mitigation is to design for the hydrogen to be rapidly mixed with the containment atmosphere so as to be diluted below flammability levels. Then, as hydrogen continues to be produced in the longer term, recombiners can be located in forced-air ducts or passive recombiners can be distributed within containment to remove hydrogen and the heat of recombination used to promote local air circulation. However, this approach does not eliminate the possible existence of high hydrogen concentrations at locations removed from the recombiners. Multi-dimensional containment fluid-flow calculations are needed to guide the positioning of recombiners and to demonstrate that localized pockets of high hydrogen concentration can be successfully avoided.

Utilizing this principle of mixing and recombination, an improved strategy, using recombiners, is to arrange for a specific overall circulation of the containment atmosphere such that the recombiners can be located immediately downstream of the hydrogen source. This would make the mixing process more predictable and solve the problem of mass-transfer of hydrogen and air to distributed recombiners. Ideally, the recombiners would be located above the hydrogen source so that the heat of recombination would assist this overall circulation. In this way, the hydrogen would be removed as quickly as possible and as close as possible to the source, thereby minimizing the amount of hydrogen immediately downstream of the source and reducing the hydrogen concentration to acceptable levels at other locations.

Such a strategy requires the containment volume to be configured into an air upflow path, past the hydrogen source and the recombiners, and an air downflow path to complete the circuit. Thus containment baffles are required and are often available in the form of crane walls or walls which separate an outer accessible volume from an inner reactor vault. Steam generator enclosures could be used for the upflow path. The flow could be generated actively using fans or passively using buoyancy forces arising from the difference in density of gases in the upflow and downflow paths; the gases in the downflow path would be cooled at an elevated heat sink.

This paper describes how such a strategy can be applied to CANDU[®] reactors.

Passive Emergency Water System PEWS

A passive emergency water system is discussed in Reference [1] and can be applied to CANDU reactors as shown in Fig. 1.

The vented water pool in the containment dome is a general-purpose emergency heat sink. For a large CANDU, a volume of about 2000 m³ provides, after boil off, a three-day heat sink. It serves as a heat sink for the steam generators, for the CANDU

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moderator acting as an emergency-core-cooling (ECC) system, and for containment. Water from the vented pool is supplied to and returns by natural circulation from the tube side of the tube banks, located at an upper elevation within containment.

The tubes are inclined to the horizontal, so that there is a preferred flow direction for the water. Water is supplied from the pool via the header at one end, and heated water is returned to the pool via the header at the other end of the tube bank. In the longer term, a boiling steam/water mixture is returned to the pool.

The containment itself is divided into an inner and an outer region, as shown in Fig. 1. Such is normally the case in contemporary CANDU containment designs, the outer region being accessible during normal operation and not connected to the inner region. However, a connection is required in this design, at least during accidents, to permit a natural circulation flow of gases up through the steam generator enclosure and down through the accessible region. The flow is induced by the difference in density of the hot gases (air, steam and hydrogen) rising from a break in the reactor coolant pipes and the gases cooled in passing across the elevated tube banks. The large elevation difference between the heat source, near the reactor, and the heat sink, at the tube bank leads to enhanced natural circulation flows of containment gases. The flows are directed by using the internal wall as a baffle which eliminates mixing between downflowing and upflowing streams.

A reduced long-term containment pressure is achieved with a high rate of heat transfer from the containment gases to the water-cooled tubes. A large heat transfer coefficient follows not only from the enhanced flow velocities but also from the high heat transfer coefficient for flow across a tube bank as compared to the coefficient for flow tangential to a surface such as a vertical containment wall.

Hydrogen Mitigation

The enhanced natural circulation flow within containment permits improved hydrogen mitigation. Hydrogen mitigation can be accomplished by directing flow to the source of the hydrogen and locating catalytic hydrogen recombiners in the stream of mixed air, steam and hydrogen. If the recombiners are located at low elevation in an upward flow of this stream, the heat of recombination acts to augment the buoyancy-induced flow.

Figure 1 shows hydrogen recombiners located in the CANDU steam-generator enclosures. The intent is to blank off any alternative flow path bypassing the recombiners so that the entire recirculating air flow is available for hydrogen mitigation. This greatly increases the effectiveness of recombiners as compared to the conventional strategy of dispersing recombiners throughout containment and relying on local convective flows for supply of air to each recombiner. LOCA calculations, using the GOTHIC code, show air and steam flow rates which would dilute hydrogen concentrations to below flammable concentrations of 4% in the steam generator enclosure, even at the inlet of the recombiners. This calculation assumes a worst case impairment of the emergency coolant injection system to maximize hydrogen production and assumes that all such

hydrogen enters only one of four steam generator enclosures. Higher local concentrations can be expected upstream of the recombiners and nearer the break but with the recombiners located so close to the break, the mass of hydrogen is small. This residual problem of hydrogen combustion in the region near the hydrogen source may require bounding or more sophisticated calculation of the effects of such ignition. Igniters might be employed in this region to limit the effects of a sudden deflagration.

Catalytic hydrogen recombiners located in a strong flow of air can have single-pass efficiencies above 80% [2] leading to small exit hydrogen concentrations. This leads to small hydrogen to air ratios in the stream at exit from the recombiners and precludes the need for any additional recombiners.

Passive CANDU-6 Application

The feasibility of these ideas, when applied to a passive CANDU containment, was demonstrated in simulations using a series of simplified GOTHIC models of a passive CANDU-6 containment. As discussed above, the containments of contemporary CANDU reactor designs are divided into an inner, non-accessible region and an outer, accessible region. In the case of the passive design they are connected, at least during loss of coolant accidents. The outer region can be arranged to include such regions as the containment dome. This was assumed in the GOTHIC code simulations which placed the dome in series with the other regions of the recirculating flow path. The objective was to show that the global long term containment behavior, such as pressure, temperature and hydrogen transients, under the worst-case scenario LOCA with impairment of the emergency coolant injection system and all decay heat being released to containment, is acceptable. Although this exercise was based on a CANDU-6 containment, with passive features such as the PEWS and elevated tube banks, similar results can be obtained for other CANDU containments that would employ such passive concepts, scaled accordingly.

The containment was divided into five principal regions, shown in Fig. 2: the upflow region (generally the fueling machine vaults and steam generator enclosures), the containment dome, the elevated tube bank region, the downflow region, and the remainder of containment. The circulation loop is completed by the downflow region being connected to the fueling machine vaults near the floor level. The hydrogen recombiners, when modeled, were located at the top of the steam generator to ensure that all possible hydrogen sources are below them (i.e. upstream in the air circulation loop). Some design changes in pipe location will likely allow the lower, more favorable location of the recombiners, shown in Fig. 1, near the bottom of the steam generators. Overall containment circulation is passive and driven by the buoyancy difference in the upflow and downflow regions, resulting from heat removal through steam condensation at the elevated tube bank. During hydrogen release this flow would be further assisted by heat generation from the recombiners, however, this effect is minimized in the model due to the high location of the recombiners.

Fig. 3 shows the break and total hydrogen inflow into the bottom of one of the fueling machine vaults (including break flow and recirculation) and the return air and steam flow rates from the downflow region into the same fueling machine vault. With a maximum hydrogen inflow of 360 moles/s and an air and steam return rate of 7200 moles/s a simple calculation shows that the maximum hydrogen concentration (well mixed) at the inlet to the recombiner would be 5 % with no recombination. If all the hydrogen can be recombined, only the break hydrogen flow must be considered in the break vault, leading to an expected fueling machine vault hydrogen concentration of below 2%. These bounding cases are shown in Fig. 4. Figure 5 shows the transient hydrogen concentrations, as predicted by GOTHIC with a recombiner model, at the inlet of the recombiners for various recombiner efficiencies, including the case of no recombiners. The recombiners are predicted to have a dramatic effect on hydrogen concentrations, even at a low assumed efficiency.

PWR Application

The idea for an elevated PEWS and hydrogen recombination within a defined recirculation flow could be applied to advanced PWR containments. The separation of the containment into an upflow and a downflow region would be done using existing walls or additional baffles, thereby directing an air-rich mixture towards the hydrogen source to facilitate recombination. Location of the recombiners in the upflow path, just upstream of possible break locations, would minimize the mass of hydrogen and optimize the recombiner efficiency, if high air flow rates can be ensured by the overall, passive recirculation or by fans.

Conclusion

Optimal hydrogen mitigation can be achieved in water-cooled nuclear reactors by arranging for an overall circulation of the containment atmosphere that directs a mixed flow of air, steam and hydrogen through recombiners located immediately downstream of the hydrogen source. In CANDU reactors, the recombiners can be located in the steam generator enclosures and the overall circulation can be achieved passively or with fans.

For passive water-cooled reactors, use can be made of inner walls which can act as baffles to direct a natural circulation flow loop of containment gases. If fans are employed, ducting can be chosen to take the flow from an air-rich region to the region of the hydrogen source to close the flow loop.

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1. Spinks, N.J., "A Passive Emergency Heat Sink for Water-Cooled Reactors with Particular Application to CANDU Reactors", 4th International Conference on Nuclear Engineering, Vol 2, p. 297, March 1996.
2. Koroll, G.W., D. Lau and W.R.C. Graham, "Catalytic Removal of Hydrogen in Humid Hydrogen-Air Gas Streams," CEC Hydrogen Behaviour and Mitigation in Water-Cooled Reactors, Brussels, Belgium, p. 317-323, March 4-8, 1991.

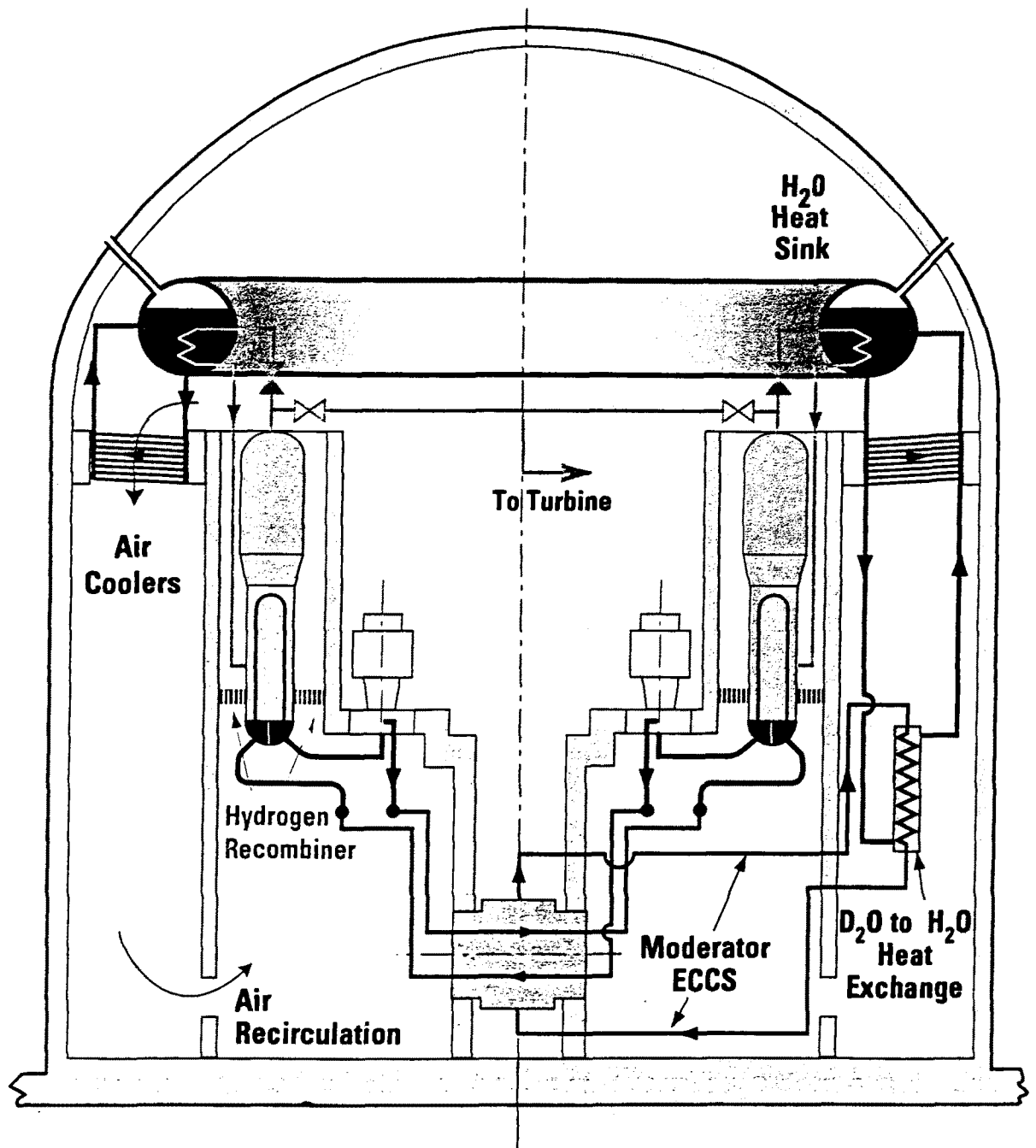


Figure 1. CANDU Containment with Passive Emergency Water System

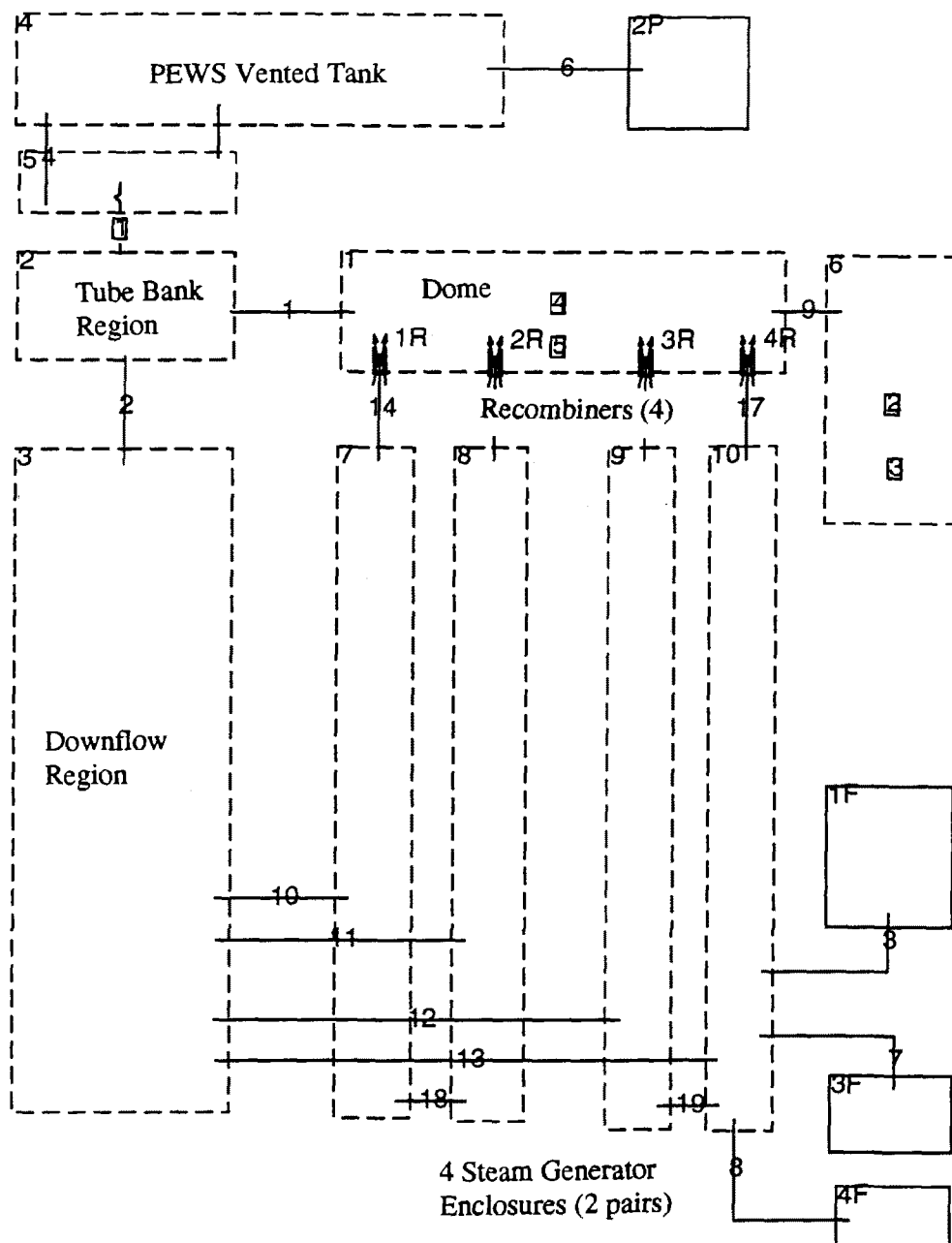


Figure 2. GOTHIC Model of Passive CANDU Containment

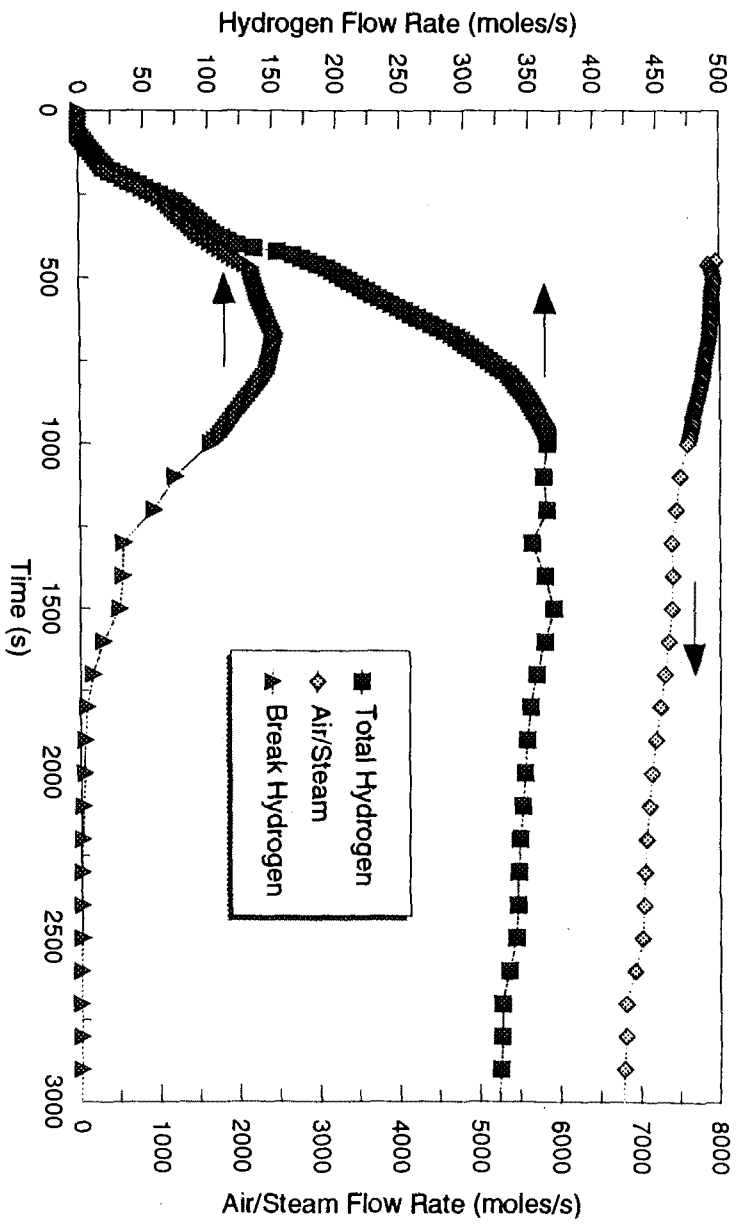


Figure 3. Hydrogen and Steam/Air Flow Rates into Fueling Machine Vault

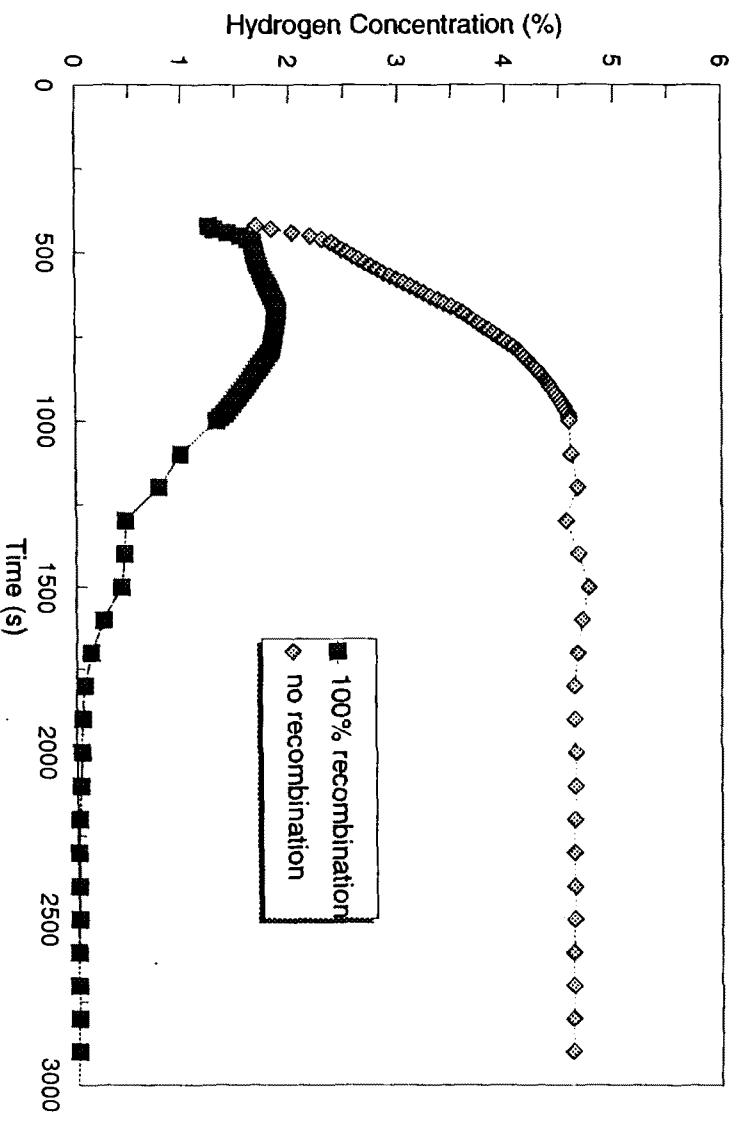


Figure 4. Hydrogen Concentration in Fueling Machine Vault

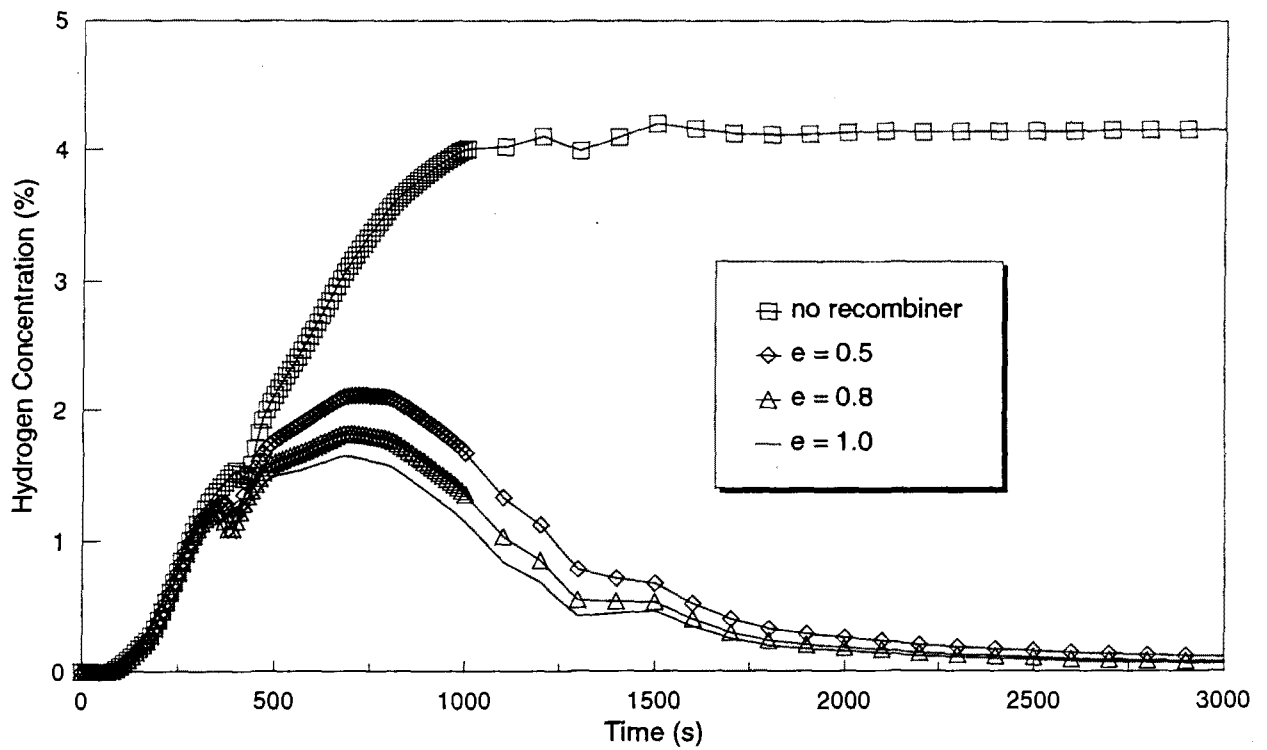


Figure 5. Fuelling Machine Vault Hydrogen Concentration with Various Recombiner Efficiencies

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6.2 CANDU 9 DESIGN FOR HYDROGEN IN CONTAINMENT

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Abstract

CANDU 9 is a single unit plant whose design is based on proven CANDU technology with some design improvements in plant performance and safety. One improvement is in the area of post-accident hydrogen control. The reactor building layout and hydrogen control system are designed to enhance atmospheric mixing and prevent unacceptably high local and global hydrogen concentrations. Preliminary safety analysis shows that a maximum of 300 kg of hydrogen is produced during the metal-water reaction phase of severe accident: a large LOCA with emergency core cooling unavailable. Even though the hydrogen production rate is conservatively overestimated, preliminary containment thermalhydraulic analysis predicts that the maximum hydrogen concentration in the accident vault peaks at 6.8% by volume without crediting hydrogen igniters and recombiners. After about one hour, the concentration throughout the reactor building is about 2.4%.

Introduction

CANDU 9 is a 925 MWe (nominal) evolutionary plant being developed for utilities with large electrical grids or high growth rates. The core and other main process systems are based on the Darlington, Bruce 'B' and CANDU 6 operating plants.

CANDU 9 has a single-unit containment that is an evolution of the CANDU 6 design. Its envelope is dominated by a leak-tight steel-lined prestressed concrete reactor building. It has two automatic isolation systems for containment penetrations, which are redundant for the ventilation isolation function. It has an atmosphere cooling system and a hydrogen control system. This paper discusses the layout and features of the CANDU 9 containment which facilitate the distribution and mixing of any hydrogen, as well as the hydrogen control system which reduces local and global concentrations.

CANDU 9 is expected to generate significant quantities of hydrogen only for severe accidents which breach the reactor cooling system followed by the failure of the emergency core cooling system. This paper discusses the generation of hydrogen from

such a postulated event, and presents a preliminary calculation of the hydrogen distribution in containment.

Containment System

System Description

The basic function of the containment system is to form a continuous, pressure-confining envelope about the reactor core and the heat transport system in order to limit the release of radioactive material to the external environment in the event of an accident. An accident which causes a release of radioactive material to containment may or may not be accompanied by a rise in containment pressure. To achieve this overall function, the containment system includes the following related safety functions following an accident:

1. isolating containment penetrations
2. reducing pressure and the amount free radioactive material inside containment
3. limiting the concentration of hydrogen inside containment
4. monitoring conditions inside containment.

The containment system includes a number of components and subsystems. The containment structure includes prestressed concrete perimeter walls and dome, as well as a reinforced concrete base slab. The inside surface of the containment structure is steel-lined for leaktightness. If the pressure or radiation level in the reactor building exceed specified limits, the containment isolation system automatically closes all of the specified process penetrations. Using a separate set of signals, but at the same setpoints, the ventilation isolation system guarantees isolation of the reactor building ventilation system. Both isolation systems are safety grade. The reactor building cooling system is used to cool the containment atmosphere and condense steam following an accident, thereby reducing the building pressure. During normal operation, some of the air coolers operate in a closed circuit dedicated to the fuelling machine vaults and steam generator enclosures. Under accident conditions, the circuit is changed such that the fuelling machine vault is ventilated with air from the accessible areas. Finally, the hydrogen control system ensures that the hydrogen concentration in any significant volume within the building is maintained at an acceptable level.

Reactor Building Layout

The CANDU 9 reactor building layout includes hydrogen control as one of the design criteria. The large free volume of the building, approximately 124 000 m³, aids in maintaining low overall hydrogen concentrations. Furthermore, in addition to the active mixing provisions included in the containment design, the reactor building layout is designed to allow passive mixing between rooms in containment. The objective is to prevent small pockets of high hydrogen concentration from forming in rooms without active ventilation.

The reactor building is nearly symmetric about its B-D centreline. The reactor vault, which houses the core (Figure 1), is located at the centre of the building at the base slab level (El. 100 m). At each reactor face is a fuelling machine vault which contains the fuelling machine and most of the primary heat transport system piping. Located directly above each fuelling machine vault is a steam generator enclosure. The fuelling machine vaults and steam generator enclosures are not accessible on-power and are atmospheric-

ally separated from the accessible areas by blow-out panels located at the top of the steam generator enclosures, as shown in Figure 2. Each of the fuelling machine vault-steam generator enclosure volumes has its own ducted air cooling system.

Around this central grouping of rooms, are located the remaining rooms in containment, most of which are accessible on-power for inspection and maintenance. Nearly all of the remaining rooms are bordered by the perimeter wall. Between the perimeter wall and the internal structures is a gap that connects the peripheral rooms to their nearest neighbours. In addition, most rooms have doors at both ends which, coupled with the perimeter gap, enhance air flow between rooms. Large volumes are also connected through grated flooring that permits buoyancy-driven vertical flow through most of the accessible areas. The largest impedance to vertical flow through the reactor building is a large floor at the 121 m elevation, shown in Figure 1. However, even at this level, there are large craneways and some grated flooring to enhance inter-room flow.

Hydrogen Control System

Introduction

The hydrogen control system is a sub-system of the containment system. Its purpose is to prevent the buildup and uncontrolled burning of hydrogen following a severe accident. An uncontrolled hydrogen burn could challenge containment integrity or damage safety related equipment. A major objective of the hydrogen control system is to disperse released hydrogen throughout the reactor building using the ventilation flows through the fuelling machine vaults. These flows mix the hydrogen throughout the entire containment volume, thereby maintaining concentrations below detonatable levels. Hydrogen igniters and recombiners are used to reduce the overall volume of hydrogen within the building and to reduce the hydrogen concentration within localized sub-volumes where mixing cannot be ensured.

Hydrogen Mixing

The hydrogen control system utilizes the large reactor building air coolers to provide mixing flows in the event of a severe accident, as shown in Figures 3 and 4. During normal operation the air coolers provide cooling for the building. There are three sets of large air coolers in the reactor building; one ducted set for each of the fuelling machine vault-steam generator enclosure volumes, and one unducted set in the reactor building dome. During normal operation, the ducted air coolers draw air from inlets at the top of the steam generator enclosure, cool it, and discharged into the bottom of the fuelling machine vault. This air then flows past the reactor face, the feeder cabinet and the steam generators back to the top of the steam generator enclosure. Each of these ducted vault cooling provides separation of the vault atmosphere from the rest of the reactor building.

In the event of a large LOCA, the pressure within the fuelling machine vault increases and causes the blow-out panels at the top of the steam generator enclosure to burst. The entire reactor building internal pressure increases until a high reactor building pressure signal activates containment isolation. The hydrogen control system is also initiated by the containment isolation signal. This includes closing the dampers in the ducts at the top of the steam generator enclosures and opening the dampers at the bottom of the hydrogen mixing ducts (Figure 2), initiating the mixing flows shown in Figure 4. Flow

is drawn into the hydrogen mixing ducts from the lowest elevation in the accessible areas, through the ducted air coolers, and then discharged into the bottom of the fuelling machine vaults. The air, along with discharging steam and hydrogen, are carried through the vaults, past the steam generators and through the blowout panels into the reactor building dome. From the dome, the air is drawn down the sides of the reactor building to the inlet of the hydrogen mixing duct. The unducted air coolers in the dome mix the air within the dome area. All of the large air coolers are environmentally qualified for these accident conditions.

Hydrogen Igniters

The hydrogen control system includes hot surface hydrogen igniters that ignite hydrogen at low concentration to prevent detonation. In this way, both the global hydrogen concentration and local concentrations in sub-volumes are reduced. The igniters are activated by the containment isolation signal and are powered by un-interruptable battery power for the required mission time. When the igniters are turned on, a thermal helical coil heats to approximately 800°C and burns the hydrogen in the immediate area. The igniter locations are chosen to ensure that local concentrations do not exceed the detonation limit. These locations are likely to be in volumes where complete mixing cannot be ensured, or in areas where large quantities are expected to be released. The igniters are placed in locations where the flame resulting from the burn will not cause damage to systems required for the safety of the plant.

Hydrogen Recombiners

Hydrogen recombiners are also utilized to reduce the quantity of hydrogen within the reactor building. Recombiners are self-contained units that use a catalyst to combine hydrogen and oxygen, producing water vapour. The recombiner has the advantage that it is passive and does not require power to start. Therefore, it does not require the containment isolation signal for activation. Recombiners operate at low hydrogen concentrations, long before the flammability limit is reached and at high steam concentrations where igniters are not effective. An additional benefit of recombiners is that the recombination reaction is exothermic and causes a convective flow through the device. This flow enhances mixing of the building atmosphere. Hydrogen recombiners are also located in areas where hydrogen mixing cannot be ensured. As well, recombiners are located in the steam generator enclosures so they can recombine hydrogen as it passes through the enclosure.

System Design

The hydrogen control system is designed to operate in the severe environmental conditions that may exist within the reactor building during a LOCA event. This includes elevated temperatures and pressures as well as high radiation. Also, the components of the hydrogen control system that may be subjected to hydrogen burn are designed to withstand that burn. The igniters and recombiners are also placed in locations where they are protected from jet impingement and pipe whip. The hydrogen control system is seismically qualified to withstand a design basis earthquake. The qualification includes the power, control and instrumentation required during the mission of the system.

Hydrogen Generation

Given below is a brief description of hydrogen generation in the CANDU 9 reactor during accidents.

Description of Relevant Systems

CANDU 9 uses the 37 element fuel bundle shown in Figure 5. Each bundle has a mass of 21.8 kg, of which 2.3 kg is Zircaloy-4 material. Zircaloy-4 is used for the cladding (0.42 mm thick) and the end plates of the fuel bundle.

The CANDU 9 core is shown in Figure 6. The fuel is contained inside 10 cm diameter pressure tubes which are 4 mm thick and 6 m long inside the core. Each pressure tube is surrounded by a calandria tube which is 13 cm diameter and 1 mm thick. There are 12 fuel bundles in each pressure tube, and 480 pressure tubes in the CANDU 9 core. Each pressure tube and calandria tube assembly is placed on a 28.6 cm square pitch in the core. Heavy water coolant at about 11 MPa_a and 300°C flows through the pressure tubes, which are part of the reactor coolant system. Heavy water moderator at about 70°C and atmospheric pressure is on the outside of the calandria tubes. The moderator is held by a 8 m diameter calandria tank. Although the calandria tank is large, its shell is only 32 mm thick due to the low pressure conditions. The moderator fluid in the calandria is penetrated by a reactivity devices for the control system and the two separate shutdown systems. The outside of the calandria is surrounded by light water shielding, also at low temperature, and a 13 m diameter shield tank.

The reactor coolant system serves as the normal heat sink for fuel heat. An emergency core cooling system (ECCS) is connected to the reactor coolant system and is ready to be initiated on a loss of cooling signal.

The moderator fluid is circulated by two pumps, each with one-half of the system capacity, which run on normal or emergency back-up electrical supply. The moderator fluid is cooled by four one-quarter capacity heat exchangers. During normal operation, the moderator system receives heat at a rate about 5% of core fission power. The shield tank water is also circulated and cooled by a separate system. The shield cooling system normally receives about 0.5% of core fission heat.

LOCA

In the event of a breach of the main heat transport system, each of the two independent shutdown systems are designed to respond quickly and automatically. For all but the largest of break sizes, the normal control by the reactivity control system would also shut down the reactor. Due to the inherent reliability of these systems, a LOCA event without reactor shutdown is an extremely unlikely event and not is considered further. The ECCS is also designed to respond automatically after shutdown to restore fuel cooling and limit any fuel heat-up. The fuel cladding temperature of the hottest fuel bundles could temporarily exceed 1000°C depending on how the break size reduces core flow during blowdown. However, the heat-up period would be brief enough that little hydrogen would be generated from the cladding-steam reaction. There would be at most 2 kg of hydrogen generated in the worst case for this accident. This amount would be generated within the first few minutes of the break.

In the long term, hydrogen may be released to the containment atmosphere from four sources:

1. Metal-steam reaction: A reaction between steam and aluminium surfaces of the feeder cabinets may produce hydrogen.
2. Deuterium outgassing: Deuterium dissolved in heavy water that spills on to the reactor building floor may be released to the containment atmosphere.
3. Radiolysis: Gamma energy absorbed by water following a LOCA produces hydrogen by radiolytic decomposition.
4. Hydrogen bottles: Although the bottles are housed outside of containment, the hydrogen inventory of the hydrogen addition system, used for heat transport system chemistry control, may be released to containment.

The hydrogen production rates from these four sources have not been computed for CANDU 9, but they are typically much lower than the rates resulting from a LOCA without ECCS, and therefore, well within the capacity of the hydrogen control system.

LOCA without ECCS

This accident is severe in that significant fuel damage would occur, much more than if the ECCS responded. The moderator system would eventually become the heat sink for the fuel heat. For that to occur, the pressure tubes must heat-up to around 700°C and deform to contact their surrounding calandria tubes. The outside of the calandria tubes would be cooled by the moderator fluid. The fuel would heat-up to over 2000°C and transfer heat to the pressure tube largely by radiative heat transfer. It is conservatively postulated that all fuel cladding would oxidize and generate hydrogen. Presuming the worst cooling conditions for the fuel after blowdown, a maximum of 300 kg of hydrogen could be generated over a period of about 30 minutes.

Containment Safety Analysis

Analysis Objectives

The primary objective of nuclear safety analysis is to predict the consequences of postulated accidents in such a way that the predications pessimistically bound reality. If the analysis objective is to predict the dispersion of hydrogen inside containment following a LOCA, then the input model, assumptions and initial conditions are tailored to overpredict containment pressure and underpredict atmospheric mixing. The purpose of hydrogen distribution analysis, at this early stage of the power plant design, is to predict the room-averaged hydrogen concentrations so that the appropriate mitigation measures can be implemented, for example, increasing atmospheric mixing and establishing the required locations of hydrogen recombiners and igniters.

Mixing of hydrogen within the containment atmosphere is predicted using containment thermalhydraulic computer codes. There are two basic approaches used in current safety analysis. In the first approach, containment thermalhydraulics is modelled by a system of one-dimensional lumped-parameter sub-volumes. Typically, individual rooms, or groups of rooms, are defined as nodes, and the flow paths between nodes (links) are modelled using hydraulic head loss equations. In a newer, more complex approach, the appropriate finite difference equations are solved on a three-dimensional grid representing all, or portions of, the containment atmosphere. Using the three-dimensional approach, fluid property gradients within containment sub-volumes can be predicted. As a first step, the one-dimensional code PRESCON2 has been used here to predict

room-averaged hydrogen concentrations following a LOCA without emergency core cooling available. PRESCON2 solves the conservation equations for individual gas species, so it can model hydrogen dispersion from the break location to the rest of containment. The code can also model heat transfer to internal walls and structures, the reactor building cooling system, break discharge flashing, and other mass and energy sources and sinks.

The containment model developed for CANDU 9 is based on the design data currently available, or on the system pedigree if specific data are unavailable. Containment is modelled using 40 nodes connected by 82 links. Most rooms have been modelled individually, but in some cases, adjoining rooms have been combined into a single node. The large ducted and unducted air coolers are modelled, but smaller coolers distributed throughout the reactor building are not since they are not environmentally qualified for this accident. The ventilation system, which provides fresh air to the reactor building, is also modelled, but isolates early in the accident.

Analysis of Hydrogen Distribution Inside Containment

The design basis event that is expected to produce the most hydrogen is a large break accompanied by a complete loss of emergency core cooling. Thermalhydraulic simulations of the heat transport system have produced break discharge and hydrogen release data that are used by PRESCON2 to predict the containment response.

In this event, heat transport coolant is discharged into one of the fuelling machine vaults. It, and the adjoining steam generator enclosure, pressurize and blow-out panels at the top of the enclosure burst, allowing pressurization of the remaining rooms. When the reactor building pressure reaches 3.45 kPa_g, the reactor is tripped (if not tripped already by heat transport system or core neutronic trips) and the containment system and ventilation system isolation signals are generated. The containment isolation signal not only isolates containment penetrations, but also causes the ducted portion of the reactor building cooling system to draw from the accessible areas through the hydrogen mixing duct, instead of the steam generator enclosures.

After the initial blow-down period, the reactor building pressure begins to decrease. The reactor building cooling system cools the containment atmosphere, reducing the pressure and temperature, and mixes the containment atmosphere.

To conservatively overpredict the hydrogen concentration inside containment, some of the analysis assumptions and initial conditions are adjusted. For this analysis, additional mass sources and sinks that allow hydrogen to escape containment or cause its concentration to be diluted are either underestimated or neglected. Therefore, the reactor building ventilation system is isolated at 0.1 s after the isolation signal is received, and the instrument air system is isolated at the time of the accident. Only one-half of the large air coolers, along with the associated fans, are assumed to be operational. Additional energy sources are overestimated and heat sinks are underestimated so that the predicted pressure and temperature inside containment represent an upper bound. Hydrogen removal by igniters and recombiners is not credited.

Analysis Results

The analysis results presented below represent the first attempt to predict the hydrogen distribution in the CANDU 9 containment. As such, they are meant to give the most

pessimistic predictions of hydrogen concentrations so that preliminary hydrogen control system design can be completed. The hydrogen source term used for this prediction, plotted in Figure 7, is a very conservative upper bound of the hydrogen production rate for this accident scenario. The figure shows that the hydrogen production rate increases rapidly at about 65 s after the accident, reaches a maximum of 182 gmol/s at 650 s (about 11 min), and then decreases rapidly. Hydrogen is assumed to be released into the fuelling machine vault as soon as it is produced.

The preliminary estimates of transient hydrogen concentrations in the important regions of the reactor building are plotted in Figure 8. The highest concentrations are found in the fuelling machine vault (accident location) and the steam generator enclosures. Like the hydrogen production rate transient, the concentration in the fuelling machine vault increases relatively quickly to a maximum concentration of 6.8% at about 830 s (13.8 min) after the accident. Much of the hydrogen that is released to the fuelling machine vault flows into the accessible areas through the steam generator enclosure. Consequently, the maximum concentration in the steam generator enclosure is nearly as large as that predicted for the fuelling machine vault and occurs about a minute later.

A relatively small opening between the fuelling machine vaults and the reactor vault is modelled to allow pressurization of the reactor vault following a LOCA in the adjoining fuelling machine vaults. In this analysis, this connection is assumed to be a 1.0 m diameter hole that is sealed during normal operation with low pressure blow-out panels. The size and location of these openings have not been finalized. Since these openings are assumed to be so small, hydrogen that flows into the reactor vault during the early part of the simulation does not readily flow into the opposite fuelling machine vault. Consequently, the maximum concentration in this room is 5.5%, and occurs at about 19 min after the accident.

The curve shown in Figure 8 for the hydrogen concentration in the dome area is representative of the concentration in the remaining rooms of the accessible areas. Figure 8 shows the concentration increasing continuously to a maximum of 2.4% at the end of the simulation. The largest increase in concentration occurs between 10 min and 40 min. After about 1 hr (3600 s), the hydrogen concentration throughout the reactor building is about 2.4%.

Summary

The CANDU 9 containment has been described from the view of controlling the hydrogen which could be generated during a LOCA without emergency core cooling available. At most 300 kg of hydrogen is expected following such an event, but the maximum production rate predicted for preliminary design assist analysis is conservatively overestimated. Both the layout and reactor building ventilation flows work to limit local concentrations of hydrogen. The highest local concentrations occur in the accident room (fuelling machine vault) and the two adjoining rooms (steam generator enclosure and reactor vault). Without crediting hydrogen igniters or recombiners, the highest local concentration is less than 7% by volume. Future, more detailed, analysis will show that the hydrogen control system effectively limits both the local and global concentrations.

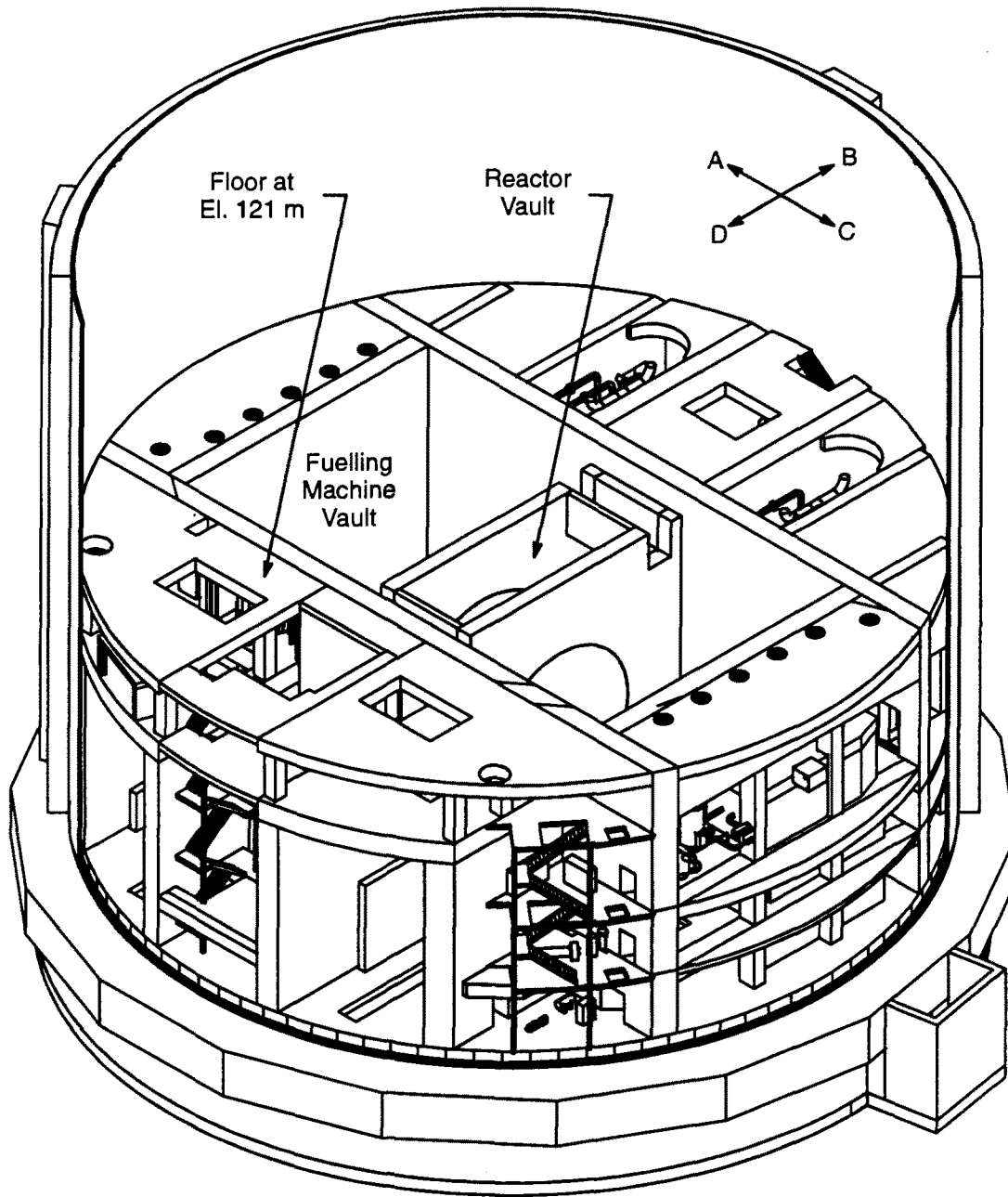


Figure 1. Reactor building during construction.

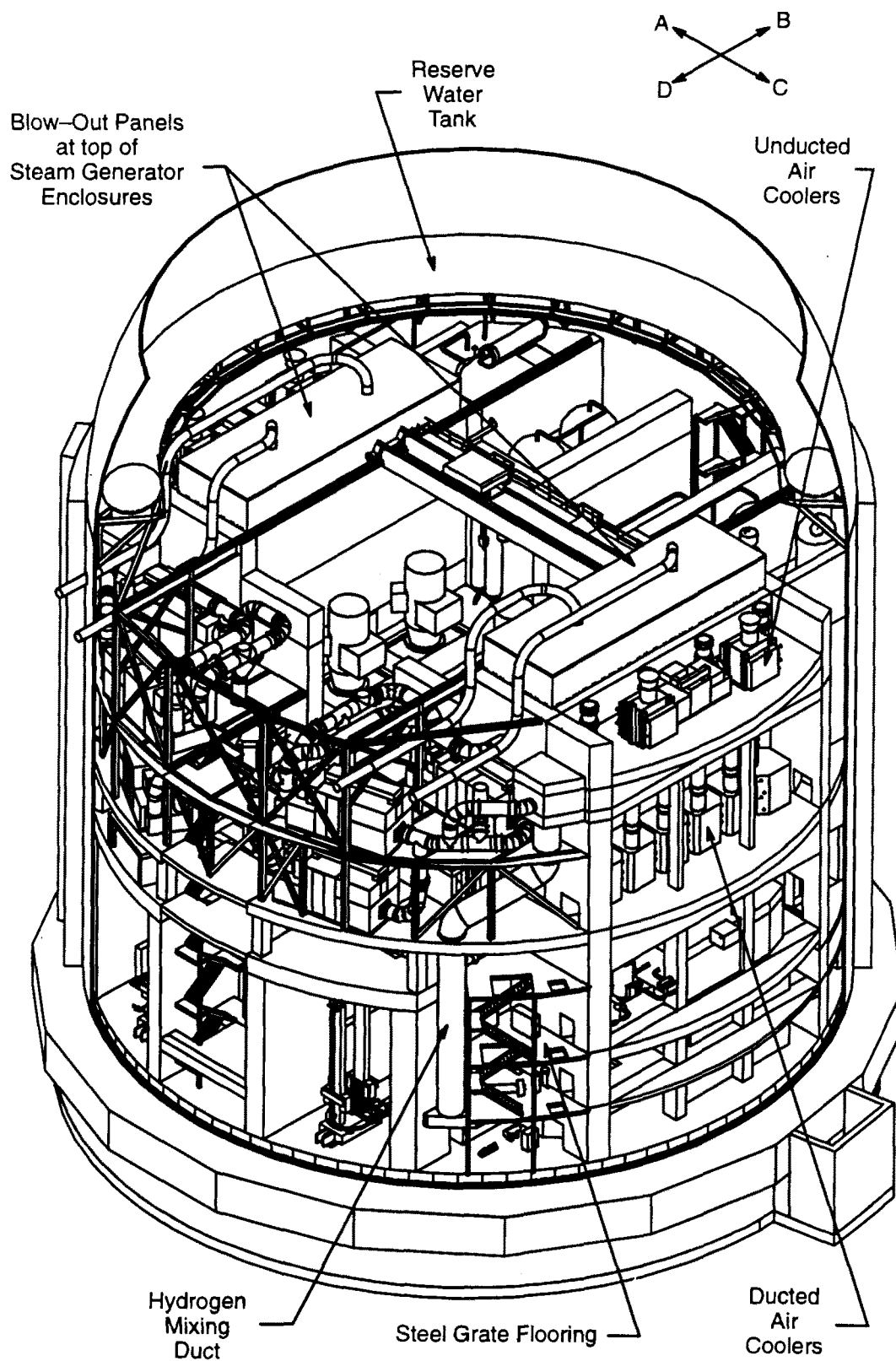


Figure 2. Cut-away drawing of the reactor building.

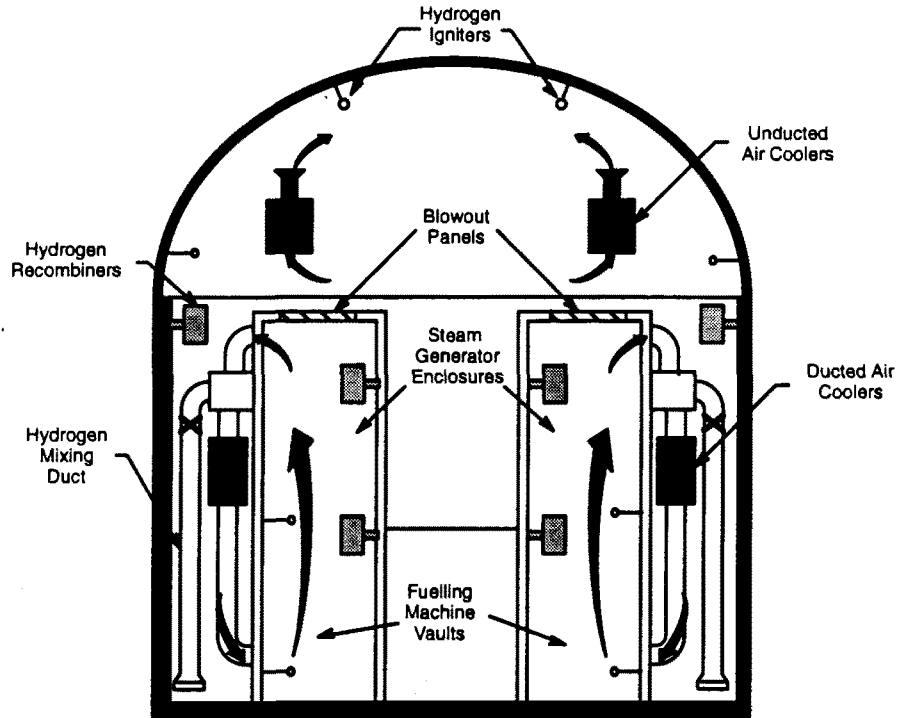


Figure 3. Hydrogen Control System – normal reactor operation.

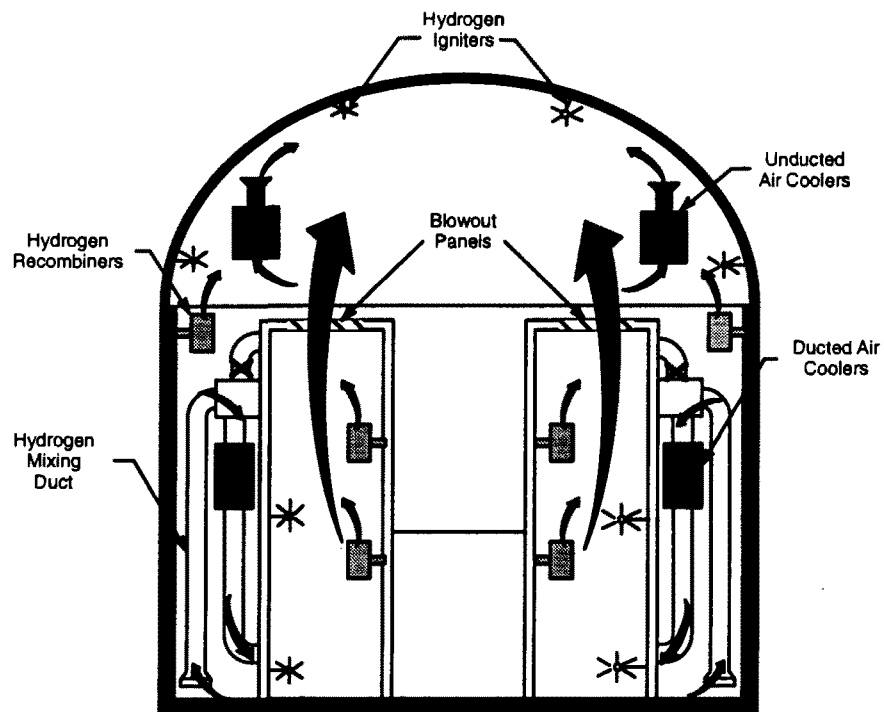


Figure 4. Hydrogen Control System – accident mode operation.

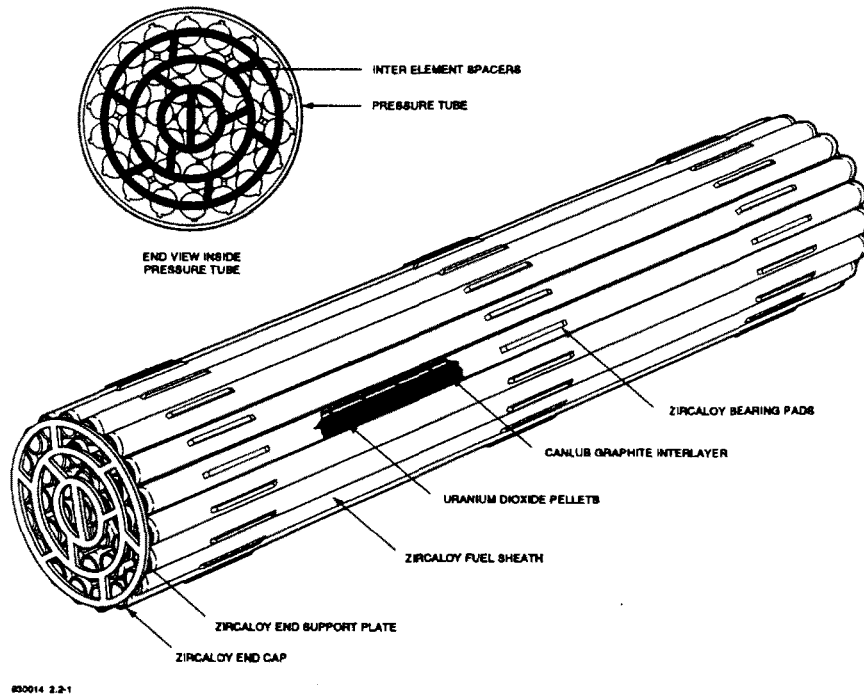


Figure 5. CANDU fuel bundle.

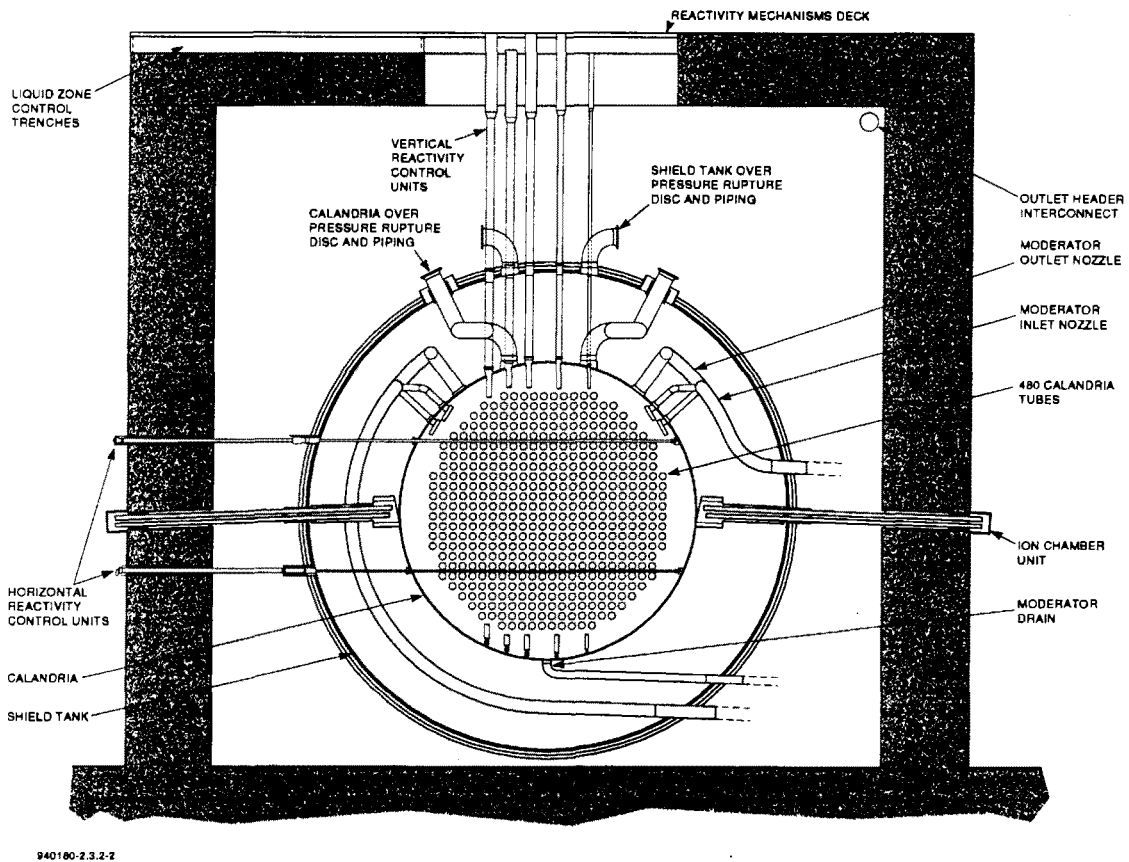


Figure 6. Front view of CANDU 9 core.

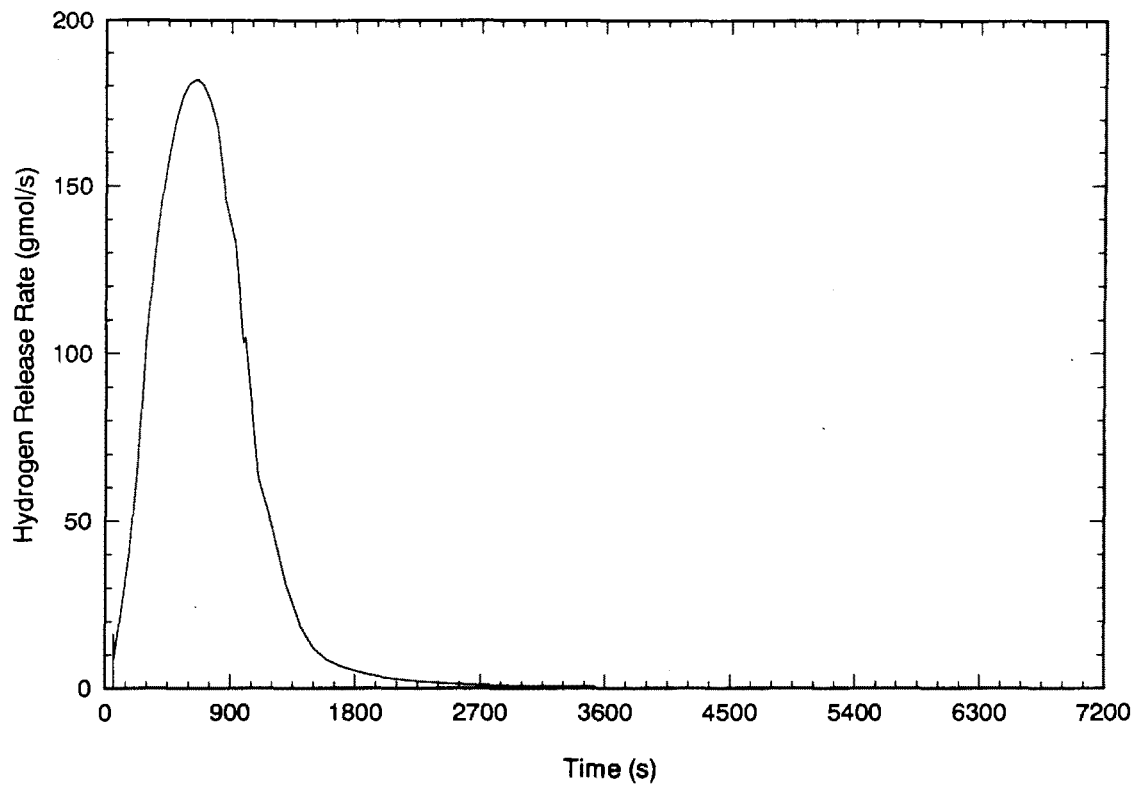


Figure 7. Preliminary estimate of hydrogen production rate following a LOCA with loss of emergency core cooling.

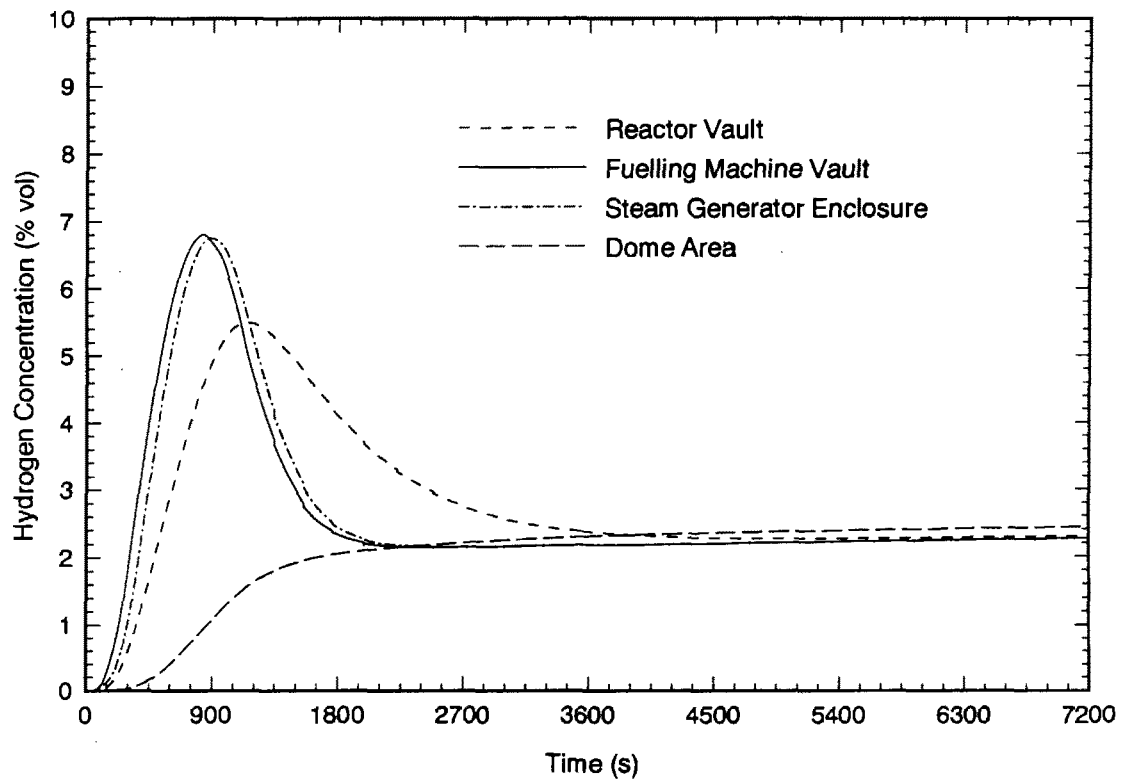


Figure 8. Preliminary estimates of hydrogen concentration in key rooms.

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6.3 PARs FOR COMBUSTIBLE GAS CONTROL IN ADVANCED LIGHT WATER REACTORS

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CA0000180

Abstract

This paper discusses the progress being made in the United States to introduce passive autocatalytic recombiner (PAR) technology as a cost-effective alternative to electric recombiners for controlling combustible gas produced in postulated accidents in both future Advanced Light Water Reactors (ALWRs) and certain U.S. operating nuclear plants. PARs catalytically recombine hydrogen and oxygen, gradually producing heat and water vapor. They have no moving parts and are self-starting and self-feeding, even under relatively cold and wet containment conditions. Buoyancy of the hot gases they create sets up natural convective flow that promotes mixing of combustible gases in a containment. In a non-inerted ALWR containment, two approaches each employing a combination of PARs and igniters are being considered to control hydrogen in design basis and severe accidents. In pre-inerted ALWRs, PARs alone control radiolytic oxygen produced in either accident type. The paper also discusses regulatory feedback regarding these combustible gas control approaches and describes a test program being conducted by the Electric Power Research Institute (EPRI) and Electricité de France (EdF) to supplement the existing PAR test database with performance data under conditions of interest to U.S. plants. Preliminary findings from the EPRI/EdF PAR model test program are included. Successful completion of this test program and confirmatory tests being sponsored by the U.S. NRC are expected to pave the way for use of PARs in ALWRs and operating plants.

Introduction

It has long been recognized that during and following a design basis loss of coolant accident, relatively small amounts of hydrogen and oxygen can be released to the

containment of a nuclear power plant. To mitigate such releases, U.S. Nuclear Regulatory Commission (NRC) regulations [1] require combustible gas control (CGC) systems to prevent volume average concentrations from reaching combustible levels. Almost all these systems in today's plants include electrically powered thermal recombiners. The surveillance and maintenance of these devices, some of which are quite complex, can be a significant operations and maintenance (O&M) cost burden.

The accumulation and uncontrolled burn of hydrogen in the Three Mile Island 2 (TMI-2) accident in 1979 focused attention on the hydrogen produced by metal-water reactions in a degraded core accident. Since then, great strides have been made in understanding the generation and control of combustible gases produced during severe accidents [2, 3]. In compliance with expanded regulatory requirements in this area, certain types of non-inerted U.S. operating plants have installed electrically powered igniter systems to control hydrogen buildup under postulated beyond-design-basis accidents (severe accidents), to prevent potential detonations at average uniform concentrations greater than 10 vol %.

PAR Description. During the past decade, a new, simpler device called a catalytic recombiner or passive autocatalytic recombiner (PAR) has been developed for cost-effective control of combustible gases [3]. PARs are stainless steel sheet metal boxes open at the top and bottom and containing many vertical flat catalytic cartridges or plates with open gas flow channels between them (PAR units from two suppliers are shown in Figs. 1 and 2). The PAR is a molecular diffusion filter in contrast to the fixed-bed particle

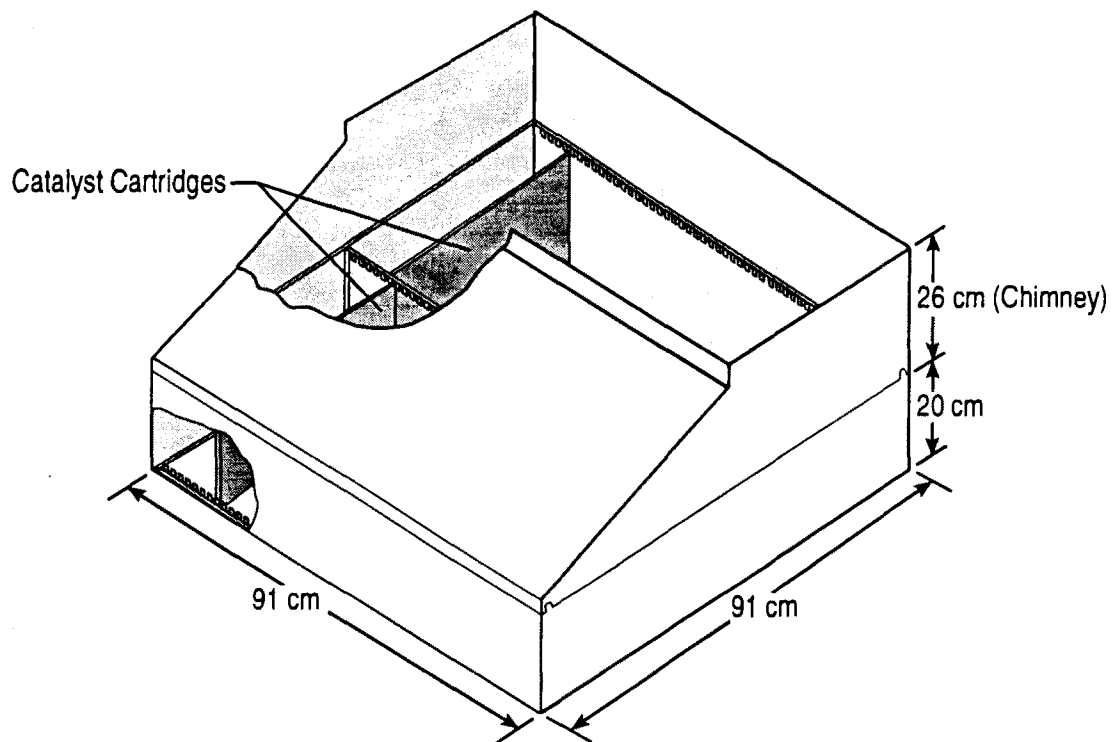


Figure 1 NIS PAR Unit

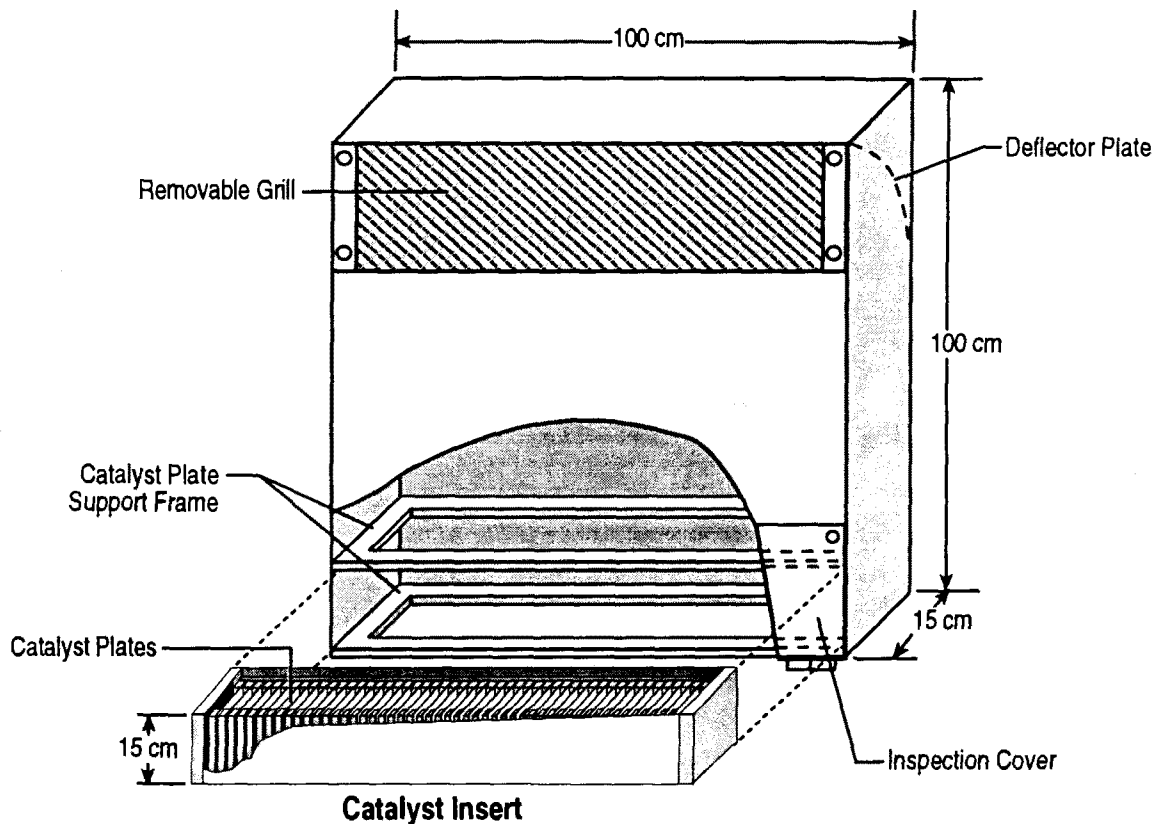


Figure 2 Siemens PAR Unit

filter configuration used in many industrial catalyst applications. During an accident the platinum or palladium catalyst in the PAR recombines hydrogen and oxygen in the flow channels to steam, which rises and is expelled from the top of the units due to buoyancy, drawing gases from the containment atmosphere into the unit from below. A "chimney" can extend above the catalytic region to provide additional lift to enhance throughput and recombination capacity. Heated gases and water vapor exhaust at the top of the unit and mix with the containment atmosphere via natural and PAR-induced convection. Under dry room-temperature conditions, the catalytic recombination process starts up almost immediately at concentrations far below flammability levels. If the PAR is wet from spray or condensed steam, startup can be delayed while the heat of recombination dries the water on the catalyst. Initial wetness can be reduced by adding a hydrophobic coating on the catalyst elements. Recombination rate increases with increasing concentrations of combustible gases and is not retarded by steam. The convection currents produced by the PAR promote gas mixing, helping to eliminate stratification. Although the catalyst material is not consumed as it functions and is not subject to long-term aging degradation, periodic surveillance is needed to detect any potential functional degradation due to buildup of contaminants during operation.

Use of PARs in ALWRs. In 1992, the Electric Power Research Institute (EPRI) recognized PARs as a favorable alternative to active devices like igniters and electrical thermal recombiners for controlling combustible gases in passive Advanced Light Water Reactors (ALWRs). PARs are consistent with the ALWR design philosophy, being simple, maintainable, and passive in nature (no moving parts; no need for cooling or electrical power, therefore invulnerable to loss of offsite power). They require no operational procedures. In 1993, EPRI changed the ALWR Utility Requirements Document for passive plants [4] to require that PARs alone (without igniters) be used to control hydrogen buildup below flammable levels in design basis accidents (DBAs) and detonable levels during and after severe accidents in PWRs (i.e. the Westinghouse AP600), and to control oxygen buildup below flammability levels in the pre-inerted General Electric Simplified Boiling Water Reactor (SBWR).

Since 1993, EPRI and ALWR designers have been engaged in the process of obtaining design certification from the U.S. Nuclear Regulatory Commission (NRC) for the use of PARs in standard ALWR designs. This paper describes the technical and regulatory history and status of introducing PAR technology into ALWRs. It also presents the prospects for backfitting PARs into certain operating plants. Finally, it gives the status of EPRI research testing being performed to enhance the understanding of PAR performance.

Overview of PAR Development and Deployment

To the authors' knowledge, four commercial designs of PARs are available, two from Germany [5, 6], one from Canada [7], and one from Switzerland [8]. They are all based on the same general catalytic technology (molecular diffusion filter) described above. Both German designs have been extensively tested. The NIS design (Fig. 1) employs palladium-coated pellets with a polymeric hydrophobic coating and enclosed in screen-covered cartridges, while the Siemens design (Fig. 2) employs a platinum-based catalyst on steel sheets with no hydrophobic coating. An extensively tested commercial version of the Canadian design has recently become available. It is similar to the Siemens design, having platinum-coated steel sheets, but the platinum is matrixed in an inorganic wetproofing support. The Swiss design is similar to the NIS design in that it employs catalyst/hydrophobic-coated pellets embedded between sheets of wire "gauze." However, the catalyst is platinum and the pellet "sandwiches" are corrugated and stacked back-to-back to form an open cross-flow channel structure. The authors are not aware of published test data on the recombination performance of the Swiss PAR design.

To date there are no PARs installed in U.S. operating plants. During 1995 Siemens PARs were installed at Doel 1 in Belgium for control of severe accident hydrogen and are scheduled for installation at six additional nuclear units [9]. Plants in Germany are considering PARs along with igniters or post-inertization for combustible gas control systems [10].

Although more than one type of commercial PAR is available for inclusion in an ALWR CGC system, a specific type employing palladium-coated alumina pellets in cartridges

and developed by NIS Ingenieurgesellschaft in Germany has been addressed in a generic EPRI PAR report to the NRC [11] and is being cited in passive plant safety analysis reports being submitted to the NRC for ALWR design certification. Once PARs are deemed as licensable via the design certification process, the final design of ALWRs can employ any type of PAR that meets design and safety functional requirements.

As required by the 1993 revision of the ALWR Utility Requirements Document [4], PARs alone can be used to control combustible gases for both design basis accidents (DBAs) and severe accidents in both non-inerted PWRs and pre-inerted BWRs. The following paragraphs describe generally how PARs alone might be deployed in ALWRs. Although based on test data and generic analyses for the NIS PAR reported in [11], the deployment scenarios would be similar for other PAR types.

Deployment in PWR ALWRs. The depletion rate of a PAR between 3 and 4 vol% hydrogen is typically greater than the radiolysis hydrogen generation rate in an ALWR PWR (e.g. a Westinghouse AP600 or ABB-CE System 80+). Therefore, for such plant designs one PAR would be adequate to maintain hydrogen levels below the DBA regulatory flammability limit of 4 vol% (dry) [11]. However, since safety-related components required to function in a DBA have to be single-failure-proof, two PARs would typically be needed to satisfy redundancy.

For severe accidents in PWRs, PARs have to control not only radiolytic hydrogen, but also the much greater amounts produced by metal-water reaction of the active fuel in a degraded core. Post-TMI regulations [12] require that hydrogen burns or detonations not compromise containment integrity, nor the ability of the plant to be brought to a safe shutdown condition. Global detonation is prevented by the requirement that the global average hydrogen concentration be less than 10 vol% (dry). ALWR containments are designed to withstand a global hydrogen burn up to 10 vol%. Local detonation in a compartment must either be eliminated by maintaining local average concentrations less than 10 vol% or be withstood by demonstrating that detonation loads will not cause failure of structures or equipment.

To keep global average hydrogen below 10 vol%, about 20 PARs are needed for a 600-MWt AP600 and about 40 PARs for a 1300-MWt System 80+. Keeping local concentrations below 10 vol% relies on designing compartments with boundaries having a large percentage of their surface open. Designs would also (1) avoid compartment configurations that support flame acceleration sufficiently to form a local detonation (e.g., long, narrow regions with obstacles and small jet openings) and (2) minimize the high-energy sources needed to initiate inadvertent ignition. If the possibility of local detonation could not be eliminated by design measures, it would have to be shown that a detonation would not cause unacceptable failure of structures or equipment.

A more detailed description of the use of PARs alone in ALWR PWRs is contained in [11, 13].

Deployment in BWR ALWRs. For BWRs with nitrogen-pre-inerted containments, it is oxygen and not hydrogen that needs to be controlled during and after an accident. PARs would control radiolytic oxygen during either a DBA or a severe accident (the hydrogen produced by metal-water reaction from a degraded core would only further inert the containment). Four full-size-equivalent PARs are distributed throughout the drywell and wetwell. (There are actually more than four PAR units, some of them being smaller than full size.) These PAR units also can function after an accident for removal of hydrogen prior to containment entry of either pre-inerted or non-inerted containments.

A more detailed description of the use of PARs in ALWR BWRs is contained in [11, 14].

Although PAR technology is applicable to both small passive ALWRs and large active evolutionary ALWRs, it is currently included only in the designs of passive ALWRs. Both evolutionary designs, the System 80+ and ABWR are interested in the benefits of PARs, but have received standardized final design approvals with other types of CGC systems. Both designs use electrically powered thermal recombiners for DBAs. For severe accidents, the System 80+ uses electrically powered distributed igniters to control hydrogen and the ABWR uses pre-inerting with nitrogen to control oxygen.

NRC Evaluation of Combustible Gas Control Using PARs Alone

In November 1994, the NRC issued the "Staff Evaluation of EPRI's Report dated April 8, 1993, "Qualification of PARs for Combustible Gas Control in ALWR Containments" [15]. The report had presented a summary of test data and simplified analysis to support the use of a PAR-only CGC system for both DBAs and severe accidents in PWR and BWR ALWRs.

The NRC evaluation stated that "The staff concludes, based on the information in EPRI's report, that PARs are acceptable devices for the control of combustible gas within ALWRs for the complete spectrum of DBA conditions." From a design-specific point of view, the evaluation stated further, "the staff will determine the acceptability of using PARs for DBA applications during its review of individual applications." Apparently the first statement meant that PARs are acceptable for DBAs from a generic technology point of view.

For severe accident applications, the evaluation stated that the following several issues need to be addressed further in order to gain NRC acceptance of PARs alone:

- Insufficient test data on PAR performance for hydrogen concentrations exceeding 6 vol%.
- Need for additional data on the potential effect of sulfur (from cable fires) and tellurium (as a core melt fission product) as catalyst poisons.
- Need for additional data on the effect of carbon monoxide (from core-concrete interaction) under BWR oxygen-deficient conditions.

- Need to perform parametric studies as part of a specific design analysis to determine how sensitive the PAR system would be to variations in severe accident hydrogen release rates.
- Insufficient test data on PAR performance in compartments with hydrogen concentrations that exceed 10 vol%.
- The potential for auto-ignition in hydrogen concentration exceeding 11 vol%.

With regard to the last three bullet items, the evaluation stated that "Igniters could be an option for those rooms in which locally high hydrogen concentrations are possible."

The evaluation also pointed out the need for a design-specific surveillance program to assure that plant operating conditions do not degrade the design function of the PAR. The surveillance program envisioned by PAR developers consists of simply removing sample catalytic elements during refueling outages and performing a benchtop acceptance test of their ability to recombine a measured amount of hydrogen.

ALWR Program Response to NRC PAR Evaluation

The NRC evaluation of PARs cleared the path to their use in passive ALWRs. During 1995, the designers of both the AP600 and SBWR adopted PARs to control combustible gases in DBAs. For the AP600 design, the PARs replaced electric thermal recombiners located inside the containment. For the SBWR, the PARs replaced electric thermal recombiners located outside the containment. Pre-inerting of the SBWR is still relied upon as the main means of controlling combustible gas in a severe accident (in a pre-inerted containment, PARs are simply relied upon to maintain radiolytic oxygen below the flammability limit as they do in a DBA).

For the control of hydrogen in a PWR severe accident, the NRC evaluation can lead to two paths regarding PAR application. One is to continue to pursue the PAR-only concept, which would require additional design analysis and testing to resolve the NRC-identified issues listed above. Although EPRI views this path as having a reasonable chance of success, the additional time and resources are not available in the ALWR program.

The other path for PWRs involves the adoption of an alternative to PARs-only for severe-accident hydrogen control. At present there are two alternatives under consideration, one preferred by the AP600 designers and the other by EPRI. The AP600 designer preference is to retain its original approach of using many distributed igniters to control severe accident hydrogen. Although the igniter system can meet regulatory requirements, EPRI prefers the alternative to PARs-only suggested in the NRC evaluation -- that is, PARs supplemented with a limited number of igniters. EPRI views this "PAR-based system" as technically superior to an all-igniter system for severe accident hydrogen control. The main technical advantage of the PAR-based system is that a large number of accident scenarios (those that do not result in high release rates) could be accommodated by the fully passive PARs, with no reliance on the igniters. There would be no concern for loss of electric power or for failure of the igniters to activate due to

component degradation or operator error. Even in the unlikely event of such failures, in the majority of accidents (in which hydrogen is released at rates low enough to allow dilution, diffusion, and mixing to preclude high local accumulations), the PARs will gradually recombine the hydrogen and altogether eliminate burns. The igniters would come into play by burning hydrogen locally only in less-likely scenarios with high release rates that could overwhelm the recombination capacity of a local PAR. Such a PAR-based system would be more harmony with the passive plant design philosophy of accommodating accidents without the need for active components. To reflect this technical preference, EPRI has initiated a change in the Utility Requirements Document that would maintain distributed PARs as the principal means of control, but require a limited number of igniters, as needed, to preclude local accumulation of hydrogen as suggested in the NRC evaluation of the PAR-only approach. The resulting CGC system is similar to the German dual recombiner/igniter concept [10], except that it requires only a few igniters, while the German dual concept has many (enough to control hydrogen without the PARs).

As this paper is being prepared, EPRI is both (1) providing technical support to Westinghouse's design application using the igniter-based CGC system, which EPRI views as adequate for safety (a final NRC safety evaluation is expected this year), and (2) developing additional test data to address the list of NRC concerns about severe-accident application of PARs and thereby providing additional support for a PAR-based CGC system (distributed PARs with a limited number of local igniters) as an approach to severe-accident hydrogen control that could be adopted in PWR ALWRs in the future.

It should be noted that, with regard to relative life-cycle costs, the major savings are achieved by replacing high-capital-and-O&M-cost active thermal recombiners with simple passive PARs. The life-cycle costs of the current AP600 igniter-based CGC system and the EPRI-preferred PAR-based CGC system are not significantly different from each other.

Backfitting PARs in Operating Plants

All U.S. operating plants have the capability of depletion of radiolytic combustible gases in a DBA using thermal recombiners (including flame recombiners) located inside or outside the containment (one plant has an active catalytic recombiner located outside containment). Post-TMI beyond-design-basis combustible gas control is achieved either by volume dilution (large-volume dry containments) or by distributed igniters (ice condenser containments and non-inerted BWRs). Since the CGC systems of all operating plants are adequate for safety, a decision to install PARs as a replacement for existing CGC devices would be based purely on economics.

Thermal recombiners outside containments, whether permanently installed or transportable, require containment penetrations, isolation equipment, safety-grade electric power and control systems, and cooling. They require a significant amount of maintenance and testing. For some BWRs and PWRs, direct replacement of complex recombiner systems with simple PARs appears technically and economically beneficial.

Several U.S. operating plants are actively considering replacement of existing thermal recombiners with PARs. For straightforward replacement of DBA combustible gas control equipment in operating plants, PARs need to be environmentally and seismically qualified to U.S. standards.

In summary, PARs are being considered by several U.S. plants as a replacement for electrically operated thermal recombiners for DBA combustible gas control. In addition, PARs are being incorporated for control of radiolytic combustible gas in passive ALWR designs, and the possibility exists that the design approach for ALWR PWRs could change in the future to rely mainly on PARs for severe accident hydrogen control, using only a limited number of igniters to prevent the buildup of hydrogen to unacceptable levels in local compartments in which hydrogen is released.

EPRI/EdF Test Program Supporting U.S. Applications of PARs

To date, a substantial amount of development and testing has been completed by others on four PAR designs (see discussion above). Hydrogen depletion rates have been measured under a wide range of hydrogen concentrations, temperatures, pressures, and steam simulating postulated German and Canadian nuclear plant severe accident conditions. In addition, the effects of various inhibitors such as water and soot (the latter from cable and oil fire) and poisons such as iodine and carbon monoxide have been tested.

To improve the understanding of PAR performance for member-utility prospective users, EPRI has initiated a PAR test program in collaboration with Electricité de France (EdF) and its contractors, Commissariat à l'Energie Atomique (CEA). The test program is being conducted at the Cadarache research center of CEA and is scheduled to be completed in June 1996. The remainder of this paper presents a description of the test objectives, test facility, test scope, and preliminary findings.

The general objective of the EPRI/EdF PAR test program is to generate supplemental PAR performance data, which, together with existing test data, will provide a comprehensive technical understanding and confirmation of the expected performance of PARs under postulated DBA and severe accident conditions in both advanced and operating U.S. plants. The broadened range of parameters and conditions are intended to include those identified by the NRC evaluation of NIS test data to be insufficient to support a safety assessment.

In particular, the comprehensive data set will provide the basis for development and validation of analytical methods for predicting PAR hydrogen and oxygen depletion rate as a function of local combustible gas concentrations and ambient temperature, pressure, and moisture conditions. These methods also need to account for the possible reduction of depletion rate due to potential poisons and the possible delay in startup of recombination due to wetness from water spray or steam condensation.

Test Facility. The EPRI/EdF PAR Test Program is being carried out in the "KALI" facility of the CEA Cadarache research center near Aix en Provence, France. Figure 3 is a sketch of the experimental arrangement. The 15.6 m³ steel cylindrical test vessel is instrumented to measure

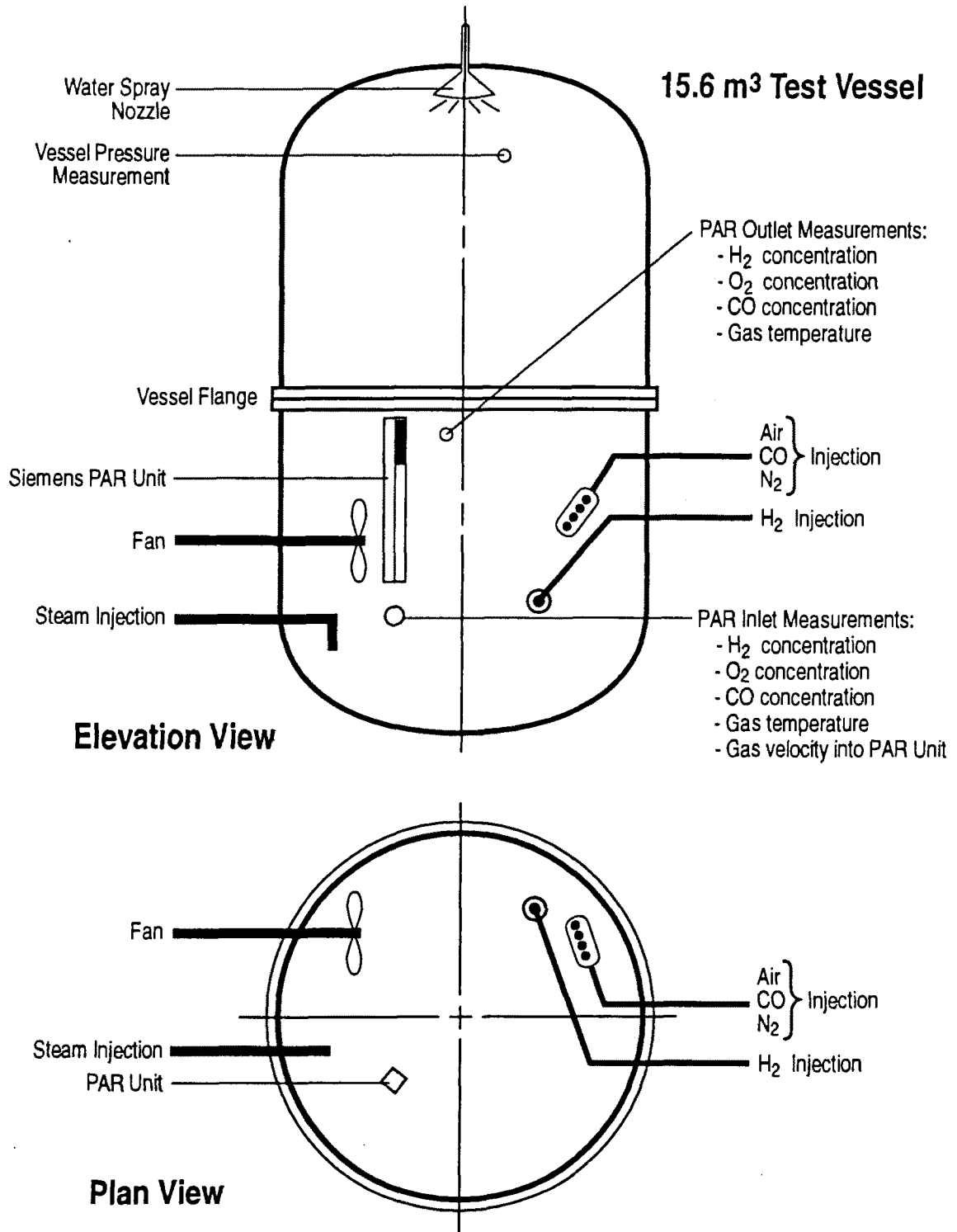


Figure 3 KALI PAR Test Facility Configuration

hydrogen, oxygen, and carbon monoxide concentrations, pressure, and gas temperatures (at two locations -- the PAR inlet and outlet). By intermittent operation of a mixing fan, vessel average concentrations of the various gaseous constituents are also measured at intervals during each test. In addition, PAR inlet gas flow velocity is recorded by an anemometer. Temperature histories are also measured at three locations on the catalytic elements within the PAR models. The facility includes the capability to inject heated air, water spray, steam, hydrogen, carbon monoxide, and nitrogen.

PAR Segment Models. Since the test facility cannot accommodate full-size PAR units, small-size "segment" models of two PAR types, NIS and Siemens, are being tested in the EPRI/EDF program. Segment models of NIS and Siemens PARs are shown in Figs. 4 and 5. The catalyst elements of NIS PARs are 20-cm-tall cartridges containing 4-to-6-mm-diameter porous ceramic spherical pellets coated with palladium and a thin hydrophobic outer layer. The faces of the 1-cm-thick cartridges consist of sheet metal with slots to permit diffusion of gases to the pellets. The catalyst elements of Siemens PARs are 15-cm-tall stainless steel plates coated with a platinum-based catalyst and ordinarily have no hydrophobic coating. Both models have 1-cm-wide gas flow channels between catalyst elements.

A PAR segment model maintains full-scale values for all key dimensions; i.e., PAR overall height, catalyst element height, and width and spacing between the catalyst elements. Only the length and number of catalyst elements are reduced from full-size values. Recombination rates are scaled up to full size by multiplying the recombination rate measured with the segment models by the ratio of full-size to small-size flow channel cross-sectional area. Using physical arguments confirmed by existing test data [10], it can be demonstrated that this approach for scale-up leads to conservatively low estimates of full-size recombination rates, in that proportionately more heat is lost to the ambient environment in the segment models than in full-size PARs.

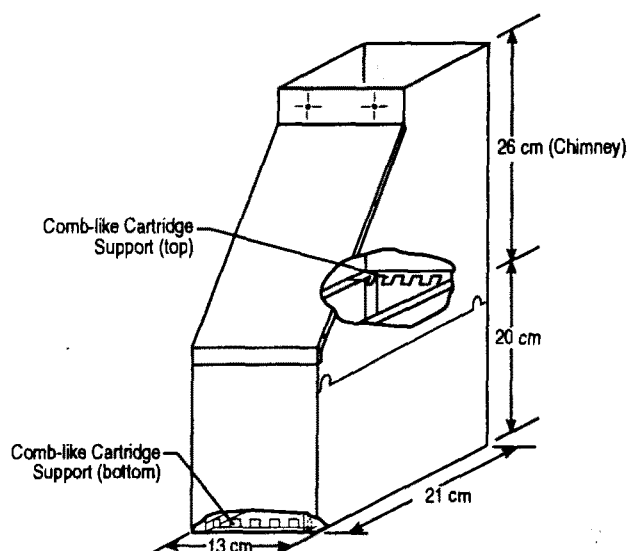


Fig. 4 NIS PAR Segment Model

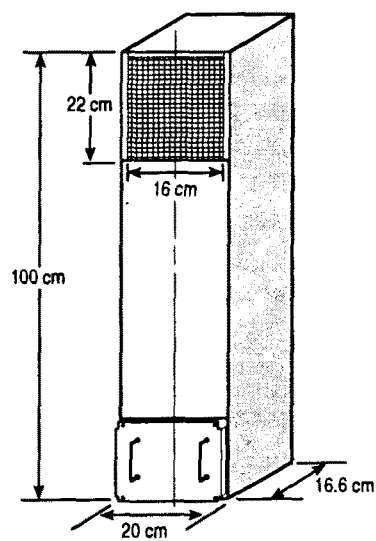


Fig. 5 Siemens PAR Segment Mode

The PAR segment models of both types are of essentially the same design and construction as the models and full-scale prototypes tested previously in Germany. Minor modifications in the NIS PAR models aim at simplifying fabrication and a minor modification in the Siemens model aims at improving start-up behavior at low ambient temperatures. The expectation that none of the modifications have a significant effect on recombination capacity will be checked in a test of each with conditions duplicating those in tests with the original design.

Test Procedure. Each test is conducted with a PAR model installed near the bottom of the vessel and halfway between the vessel centerline and wall (see Fig. 3). First, the test volume is temperature conditioned by means of heated air or steam. Then all gaseous constituents except hydrogen are added and well mixed with the fan. The data acquisition system is activated, followed by a relatively fast injection of hydrogen up to the target test concentration. Fan operation is continued for two minutes to ensure complete mixing and allow an accurate measurement of the initial volume average hydrogen concentration. Fan operation is then terminated allowing PAR heatup to establish convective flow through the PAR model. At ten-minute intervals, the fan is operated for one minute to allow measurement of test vessel average gas concentrations. Hydrogen, oxygen, and, where applicable, carbon monoxide concentrations are recorded continuously until no further change in combustible gas concentration is observed.

Test Matrix and Rationale for Selection of Test Conditions. Table 1 presents the matrix of tests planned to be performed on each of the two PAR model types. Test conditions were selected generally to represent both DBA and severe accidents in both non-inerted PWRs and inerted BWRs. The following effects/technical issues are addressed:

- a) Low ambient gas temperature as may occur in some postulated accidents; e.g. low initial containment temperature or coolers remain in operation.
- b) Effect of low hydrogen concentration (4 vol% and less) on PAR start-up time, especially in a DBA.
- c) Effect of wetness on start-up time (the wetness would come from condensation for any plant type and also from sprays in plants equipped with them -- passive ALWRs do not have sprays). Much of the data obtained previously for wet PARs was under relatively high temperature conditions (50-100°C). The EPRI/EdF tests are exploring the cold (30°C) and very wet (water soak of 1 hour) initial conditions representing a limiting case for PAR start-up delay.
- d) Effect of high hydrogen concentration (10 vol%), which is the regulatory upper limit with respect to volume average concentrations that may produce detonations in severe accidents.
- e) Effect of ambient gas temperature.
- f) Effect of ambient gas pressure.
- g) Effect of steam as inertant.

Table 1 EPRI/EdF PAR Test Matrix

Test No.*	Plant Type Simulated	Initial Gas Temp (°C)	Initial Gas Pressure (Bar)	Initial Gas Composition (vol %)**					Remarks
				H ₂	Air	Steam	CO	Excess N ₂	
1	PWR	30	1.3	2	98	0	0	0	Low H2 conc (DBA)
2	PWR	30	1.3	2	98	0	0	0	Effect of wetness on start up ***
3	BWR	30	1.3	8	18	0	0	74	Effect of wetness on startup (low O2)***
4	BWR	30	1.3	8	18	0	0	74	Low O2 (BWR)
5	SIEM NIS	112 100	2.9 2.0	5 3.6	45 46.4	50 50	0 0	0 0	Repeat of test previously conducted in Germany on PAR developmental designs
6	PWR	30	1.3	4	96	0	0	0	Effect of initial H2 concentration (DBA)
7	PWR	100	1.3	4	96	0	0	0	Gas temperature effect
8	PWR	30	4.0	4	96	0	0	0	Gas pressure effect
9	PWR	119	4.0	4	46	50	0	0	Steam Effect
10	BWR	63	1.3	8	18	18	0	56	BWR wetwell conditions
11	BWR	115	4.0	4	10	44	0	42	BWR drywell conditions
12	PWR	30	1.3	4	93	0	3	0	Inject CO when H2 reaches 2.5%
13	BWR	30	1.3	8	18	0	3	71	Inject CO when O2 reaches 3 %
14	PWR	30	1.3	10	90	0	0	0	Effect of initial H2 conc (Sev Acc)
15	PWR	30	1.3	4	96	0	0	0	Potential poisons from US cable fire
16	BWR	30	1.3	8	18	0	0	74	Potential poisons from US cable fire

* Matrix to be tested separately for NIS and Siemens PAR units

** After H₂ injection. "Excess Nitrogen" means in addition to the nitrogen in air.

*** Sprays (without chemicals) to be activated (30 °C) for 1 hour and then terminated prior to H₂ injection to ensure a fully wetted PAR

- h) Effect of nitrogen inertant typical of a BWR.
- i) Effect of low oxygen concentrations to simulate inerted BWR conditions.
- j) Effect of potential poisoning from carbon monoxide that would be released from core/concrete interaction late in a severe accident.
- k) Effect of potential poisoning from U.S. cable burn products such as carbon, sulfur, and hydrochloric and sulfurous acids (from chlorosulfonated polyethylene jackets).
- l) Effect of minor differences between the PAR types in this program and original versions tested previously.

Previous testing in Germany has established the effect of fission product iodine on PAR depletion rate (the depletion rate is reduced, but not substantially enough to incapacitate the PAR's recombination function). Although not specified in the test matrix of Table 1, an additional KALI test on both PAR types to examine the effects of airborne tellurium (a fission product potentially poisonous to catalytic recombination) is being considered.

Preliminary Observations

At the time this paper was completed, testing of the Siemens PAR segment model was mostly completed and testing of the NIS PAR was in progress.

Figures 6 and 7 are typical examples, for the Siemens PAR, of measured histories of wet hydrogen concentration and PAR flow velocity variation within the test vessel, respectively. For this test the nominal initial conditions are 5% hydrogen, 45% air, 50% steam, 2.9 bar, and 112°C. Note from Fig. 6 that hydrogen recombination begins immediately following hydrogen injection and continues until the hydrogen concentration is reduced to approximately 0.25 vol%. Vessel average concentration is recorded at the end of each intermittent fan operation. This record will be analyzed to give PAR model depletion rate as a function of concentration.

The spikes in flow velocity exhibited in Fig. 7 are the result of intermittent fan operation and are not true PAR flow velocities. Accurate flow velocities (ranging from 0.2 to 0.4 m/s) are obtained between 1200 and 3700 seconds. The threshold for flow velocity measurement appears to be approximately 0.2 m/s. The output is zero for velocities below the measurement threshold.

Based on a preliminary quick look at the **Siemens PAR model data** obtained to date, the following observations are made (except where noted, results are for Siemens PAR models without hydrophobic coating). The term "start up delay time" denotes the time from the beginning of hydrogen injection to the time when a significant depletion of hydrogen or oxygen is observed.

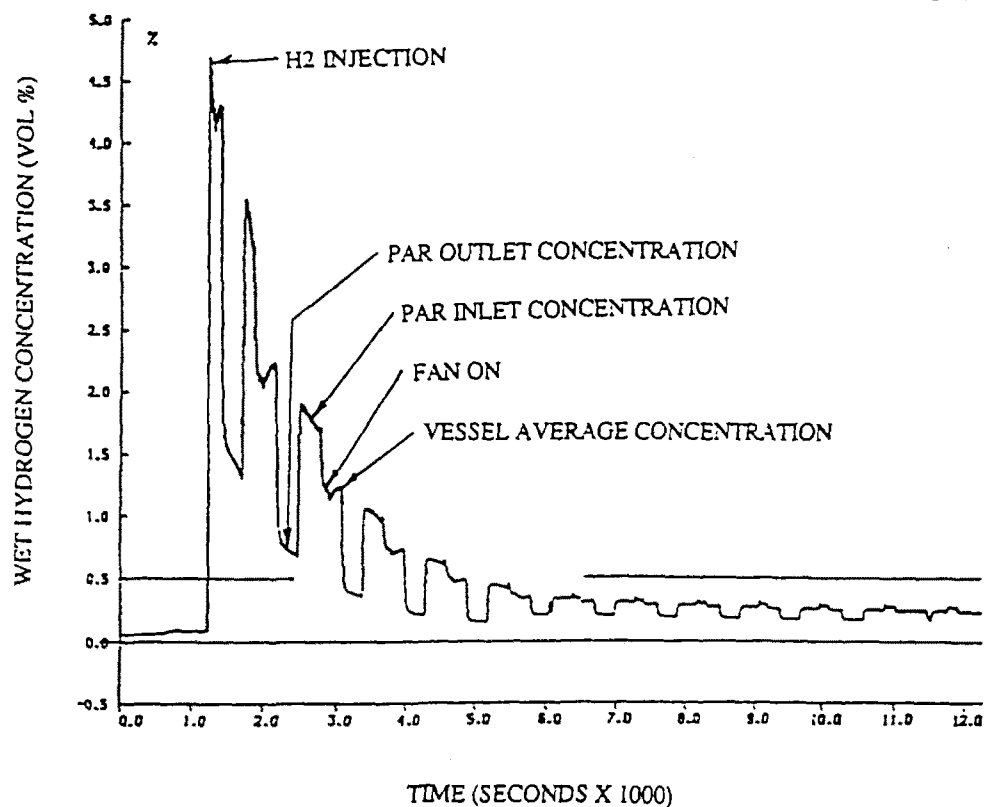


Figure 6 Hydrogen Concentration History (Test S5)

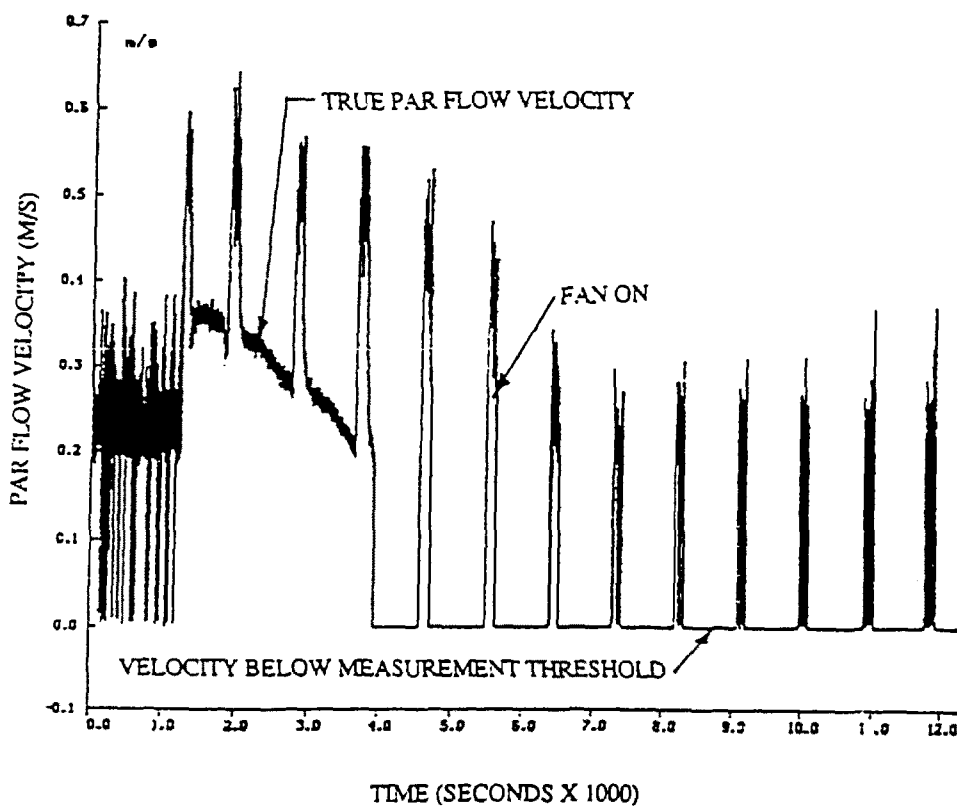


Figure 7 PAR Flow Velocity History (Test S5)

- 1) Under dry, cold (30°C) conditions, immediate PAR start up occurred at the lowest initial hydrogen concentration tested (2.0 vol%).
- 2) Once the PAR started up, recombination continued until the hydrogen concentration dropped below approximately 0.25 to 0.5 vol%.
- 3) Under warm (100°C) conditions in a steam-rich condensing environment with a hydrogen concentration of 4 vol% immediate PAR start up was observed.
- 4) Under cold (30°C) and very wet conditions (cold spray water applied for 1 hour prior to test initiation), PAR startup delays at low hydrogen concentrations can occur while water is being heated off. Under such initial conditions, a start-up delay of 45 minutes was observed at 3 vol% hydrogen and of more than 4 hours at 2.5 vol%. At 2 vol% no startup was observed after 5 hours and the test was terminated.
- 5) Under cold (30°C) and very wet conditions and at low BWR-inerted oxygen concentrations (3.8% oxygen, 8% hydrogen), PAR start up was observed after a 5-minute delay.
- 6) With hydrophobic coating applied to the catalyst plates, start up performance was improved; i.e., at 2 vol% start-up occurred within 12 minutes.

Based on a preliminary quick look at the **NIS PAR model data** obtained to date, the following observations are made (note that the catalytic pellets in these tests had a hydrophobic coating).

- 1) Under dry, cold (30°C) conditions, immediate PAR start up occurred at the lowest initial hydrogen concentration tested (2.0 vol%).
- 2) Once the PAR started up, recombination continued until the hydrogen concentration dropped below approximately 0.25 to 0.5 vol%.
- 3) Under cold (30°C) and very wet conditions (cold spray water applied for 1 hour prior to test initiation), PAR start-up delays at low hydrogen concentrations can occur while water is being heated off. Under such initial conditions, a start-up delays of 5 minutes (at 2 vol% hydrogen) and 37 minutes (at 1 vol%) were observed.
- 4) Under cold (30°C) and very wet conditions and at low BWR-inerted oxygen concentrations (3.8% oxygen, 8% hydrogen), PAR start up was observed after a 5-minute delay.

The observed delays in PAR start up are of no consequence in a design basis accident because combustible gas production rates are extremely low and, without any mitigation, flammable levels typically are not reached for days.

Summary

PARs appear to provide a cost beneficial approach for controlling combustible gases during and after postulated accidents in ALWRs. Acting in combination with igniters or pre-inerting, PARs deplete hydrogen in non-inerted containment atmospheres and oxygen in inerted atmospheres, such that no detonations or uncontrolled burns take place that could cause failure of safety-related structures or components. Design certification of the AP600 is serving as the arena for gaining licensing acceptance by the U.S. NRC for PAR applications in ALWRs. It is expected that an application for backfit of PARs in an operating plant will be submitted to the NRC this year. The research office of the NRC is presently conducting a confirmatory test program on PARs at Sandia National Laboratory. It is anticipated that the extensive performance test data generated by PAR developers, complemented with the EPRI/EdF and NRC test data, will pave the way for licensing acceptance of PARs for combustible gas control in both ALWRs and operating plants by year end.

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6.4 CALCULATIONS CONCERNING THE CAPABILITY OF PASSIVE RECOMBINERS TO CONTROL HYDROGEN CONCENTRATION IN THE CONTAINMENT OF AN ADVANCED PWR

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Abstract

The Department of Mechanical and Nuclear Constructions of the University of Pisa has developed a computer code, HOCRA, which is able to make an initial evaluation of the capability of catalytic recombiners to remove hydrogen from the atmosphere of the safety containments of nuclear reactors in accident conditions. The code allows the analysis of the average concentration transient of hydrogen in a generic compartment of a safety containment in a nuclear reactor. The software is structured into two groups. The first, mode-1, analyses the average concentration in all the free volume of the containment before a possible venting; whereas the second, mode-2, analyses the average concentration transient in a containment compartment, assuming input and output flow rates into and from the compartment itself. The first part of this paper outlines the physical and mathematical model of the code, the second part reports calculations made for an advanced PWR in cooperation with ENEL.

1. Introduction

Combustion cannot propagate in a gas mixture of fuel and oxidizer if at least one of the two reactants is below its flammability limit. In order to maintain the containment atmosphere non-flammable during an accident in a nuclear reactor, hydrogen and oxygen can be recombined with thermal or catalytic devices.

Recombining means that H_2 and O_2 can react significantly even at low temperatures and non-flammable concentrations, but in any case much more slowly than in a deflagration, so that the energy production rate is lower and cannot substantially affect the pressure in the containment. On the other hand, for the same reason, recombining cannot in itself cope with a high hydrogen discharge rate.

If, in spite of this mitigation system, the gas mixture composition goes over the flammability limits, recombining may also be useful for avoiding strong explosions.

Recombiners can increase the chances of success of deliberate ignition, by reducing the number and size of possible pockets of more reactive mixtures and by limiting ignitor location to the zones where it is more likely that H_2 concentration increases too fast to be controlled by recombining.

The new passive autocatalytic recombiners (PARs) that are currently being marketed by the NIS Company, Fig. 1, and Siemens ('small', 'medium', and 'large' types), Fig. 2, are simple and reliable devices without moving parts

and require no source of power. They can begin to remove hydrogen before flammable conditions are reached, also when the environment is inerted by steam.

Tests also highlighted that the working of the PAR is not substantially altered by the presence in the atmosphere of poisons or common catalytic inhibitors (aerosols, liquid drops, iodine, carbon monoxide) since the catalyst is never completely deactivated, even if exposed for several days, and the temperature reached by the catalyst has a "self-cleaning" effect. The recombiner seems to reach the steady-state quite rapidly, especially if the catalyst is coated with a water-repellent film.

PARs seem to be able to cope with higher generation rates of hydrogen better than the old active thermal recombiners, because more units can be installed at the same cost and in the same space.

Nevertheless, there is still some concern about their behavior in particular conditions typical for severe accident and/or for containment compartments. There are some difficulties in sizing the hydrogen control system and, in particular, in determining the PAR number and location for specific plant designs. To solve them by analysis, it is necessary to develop appropriate tools able to simulate the PAR operation and performances in the actual primary containment accident conditions. It is important to note that these conditions

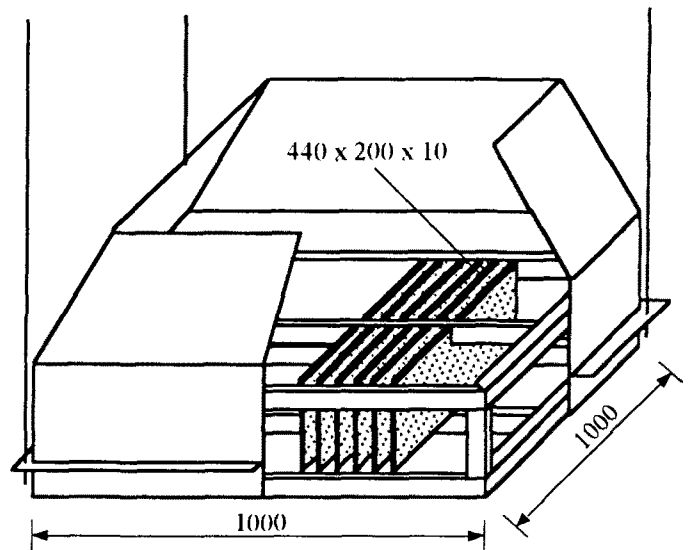


Figure 1. NIS catalytic recombiner

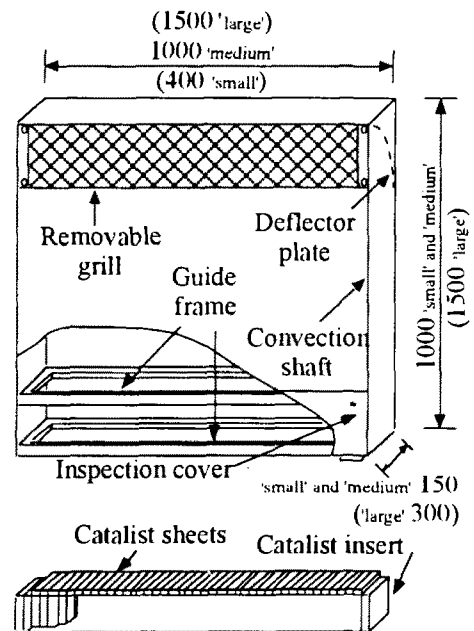


Figure 2. Siemens catalytic recombiner

are different from the conditions in which the PARs are normally tested.

Empirical correlations were written by the Department of Mechanical and Nuclear Constructions of the University of Pisa in the framework of the European H2 Project (1992-1995) [1-3], to evaluate the capability of the catalytic recombiners. On the basis of these correlations, a computer code, called HOCRA (HOMogeneous Condition Recombiner Assessment), has been created to calculate the decrease in H_2 concentration due to any number of recombiners which are installed in a volume where the balance is known between inflow and outflow rates of all the different gas components. In cooperation with ENEL, which is the national electricity board in Italy, some calculations were carried out to verify the possibility of controlling the hydrogen concentration with PARs in an advanced PWR containment during severe accident scenarios.

2. The mathematical model

In order to evaluate the average concentration transient of hydrogen in a given volume in the presence of catalytic recombiners, the physical and chemical processes that take place in the catalytic oxidization of H_2 need to be defined, along with the quantities that influence the phenomenon and the correlation between these quantities and the overall recombination rate. In fact, it will thus be possible to correctly formulate the system of differential equations (balances of mass, momentum, and energy), and by integrating these the evolution of the phenomenon in time will be given.

2.1. The recombination rate

The gas that flows through a passive recombiner is heated due to the exothermic H_2 - O_2 reaction that takes place within. The increase in temperature and thus the change in gas density depend on the quantity of hydrogen that recombines in the unit of time (recombination rate), which represents the heat source, and on the cooling capacity of the gas that passes through the recombiner. The driving pressure due to the difference in density between the inside and the outside must be counterbalanced by the pressure loss of the gas in the recombiner, due to friction and the increase in kinetic energy in the gas.

The power generated by the recombiner influences the circulation of air in the environment where the recombiner is located, which also depends on the geometry of the environment, on the location of the recombiner, and on the natural and forced convection due to any other cause.

In feedback, this circulation then affects the power from the recombiner because it affects the thermal-hydraulic conditions of the inlet gas, on which the working of the recombiner depends; specifically, the velocity, pressure, temperature, and chemical composition.

A complete and reliable simulation of the behavior of a recombiner inside the safety containment of a nuclear reactor can thus only be made by joining two models together:

- the first, or 'external' model, expresses the variation in the thermal-hydraulic conditions of the gas while it flows throughout in the environment where the

recombiner is located, after it has left the recombiner and before going back into the recombiner itself

- the second, or 'internal' model, expresses the change that the same quantities inside the recombiner undergo

The current models [4-10] do not totally satisfy these requirements.

The problem can be considerably simplified if the volume where the recombiner is located is perfectly stirred. In this case a unique empirical correlation can link the recombination rate to the thermodynamic conditions in the volume.

Quasi-steady-state recombination rates were evaluated by the recombiner manufacturers on the basis of experimental tests [4-5]. They are supplied as empirical functions of the volumetric concentration of hydrogen and of the pressure in the volumes where the recombiners were installed during the tests. These evaluations of the recombination rate of passive recombiners are based on experimental data of decreases in hydrogen concentration, obtained during recombiner operations in relatively small volumes. The composition of the gas mixture was almost uniform in the test vessels, because of the convective flow generated by the recombiner itself.

An analysis of these empirical functions showed that the recombination rate (R_r) might only be, although very roughly, proportional to the molar density of the deficiency reactant (hydrogen in the tests), Fig. 3 [8], and so be practically independent of pressure, temperature and inert gas concentration.

The correlation chosen in HOCRA to connect R_r to the experimental variation of $[H_2]$ is the following function:

$$R_r = A [H_2]^C \quad (1)$$

where A and C are the constants, whose values are derived from a best fit of the experimental data (Fig. 3) and differ depending on the type of recombiner (NIS, Siemens large, medium or small).

In advanced PWRs, the free volume in the safety containment is such that, even if a hydrogen mass is considered that would be produced by the oxidation of all the zirconium contained in the reactor core, the average composition of the mixture in all the containment would still be hypostoichiometric. On the other hand, in the individual compartments of the containment, transient atmospheres may be verified with a hyperstoichiometric H_2 concentration.

Hyperstoichiometric mixture conditions were not sufficiently tested, but it is reasonable to assume that the recombination rate is influenced by the average molar density of O_2 . In order to estimate the removal rate of H_2 even in hyperstoichiometric mixture situations, it was assumed that the recombination rate is connected in all conditions to the molar density of burnable H_2 ($= [H_2]$ for hypostoichiometric mixtures and $= 2 [O_2]$ for hyperstoichiometric mixtures). Under this hypothesis, in the hyperstoichiometric field, correlation (1) becomes:

$$R_r = A (2[O_2])^C \quad (2)$$

where A and C have the same values as in the hypostoichiometric case.

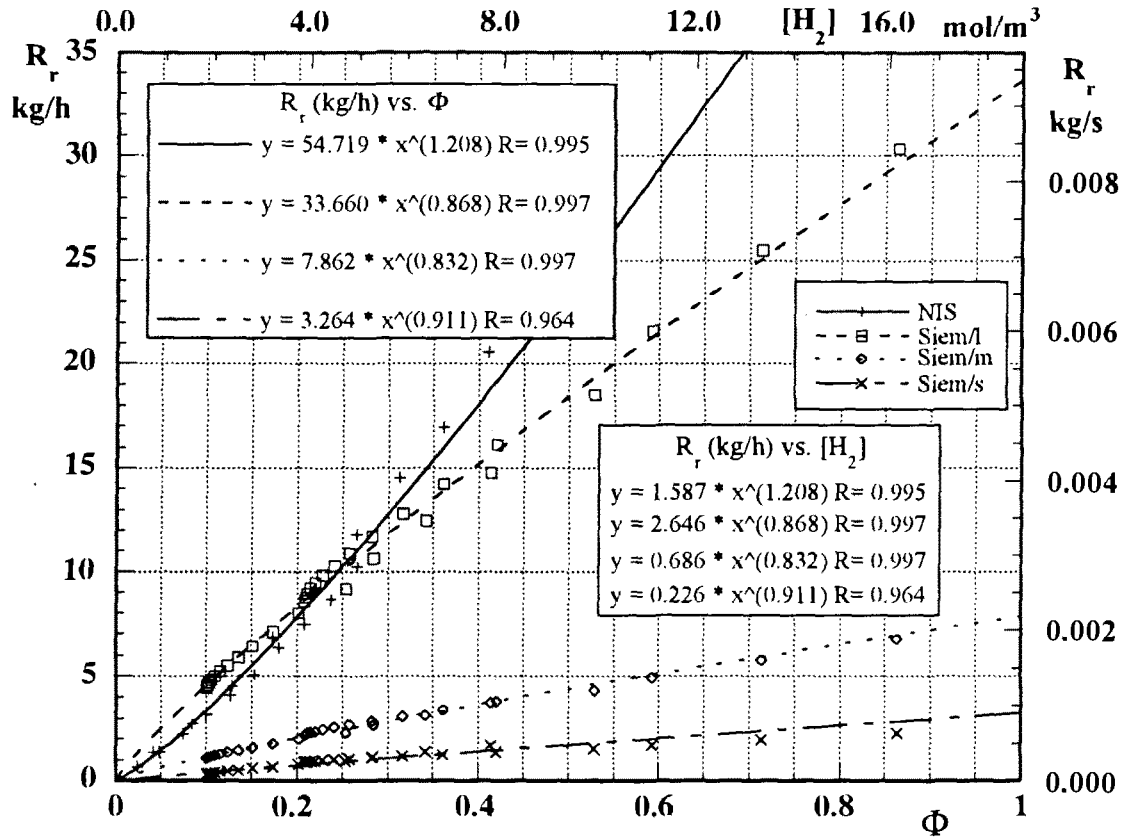


Figure 3. Recombination rate vs. $[H_2]$ in a stirred environment

2.2 HOCRA mode-1: uniform mixture in the containment

In a volume in which the thermodynamic conditions are assumed to be uniform, which may also result from the action of the recombiners which facilitate mixing within the containment, the study of the concentration transients of hydrogen and oxygen can be approached using the following mass balances:

$$\frac{d[H_2]}{dt} = \frac{J_{i,H_2} - J_{o,H_2}}{V_t} - \frac{N_t}{V_t} \frac{R_r}{M_{H_2}} \quad (3)$$

$$\frac{d[O_2]}{dt} = \frac{J_{i,O_2} - J_{o,O_2}}{V_t} - \frac{N_t}{V_t} \frac{R_r}{2M_{H_2}} \quad (4)$$

This situation is assumed by HOCRA mode-1 for the whole free volume of the safety containment of a nuclear reactor before venting. In order to calculate the average transient concentration of hydrogen, it thus requires only knowledge of the hydrogen release rate in the accident situation, in so far as the containment is closed and the oxygen

release due to radiolysis may be neglected ($J_{i,O_2} = J_{o,H_2} = J_{o,O_2} = 0$). The hydrogen release rate can be derived by containment codes.

2.3 HOCRA mode-2: non-uniformity conditions

HOCRA mode-2 evaluates the average hydrogen concentration in a volume where, although the temperature and concentrations are not uniform, they nevertheless allow a subdivision into sub-volumes, each of which is perfectly mixed. In a nuclear reactor containment, the sub-volumes may reflect the physical compartments of the containment, but more generally, they define the areas in which the thermodynamic coordinates of the mixture can be considered uniform.

The H_2 and O_2 transients in a sub-volume are still described by equations (3) and (4) where now all the quantities concern that sub-volume.

The change of the average H_2 molar density in the containment is then given by the following:

$$\frac{d[H_2]}{dt} = \frac{J_{i,H_2,t} - J_{o,H_2,t}}{V_t} - \frac{1}{V_t M_{H_2}} \sum_{j=1}^{NVI} N_j R_{rj} \quad (5)$$

where the index t refers to the overall conditions (with reference to the total volume V_t), whereas j refers to the j -th of the NVI sub-volumes in which V_t is divided.

HOCRA mode-1 would give a correct assessment of the $[H_2]$ transient, even if the thermodynamic conditions in the containment volume were not uniform, only if (i) the type and spatial distribution of the recombiners were the same in all the volume, (ii) the exponent C in correlations (1) or (2) were equal to 1, and (iii) the mixture were hypostoichiometric, or hyperstoichiometric, in the whole volume.

Since the recombination rate of the recombiners currently on the market is simulated in HOCRA with a power function in which the exponent $C \approx 1$ (Fig. 3), the results obtained with HOCRA mode-1 could in many cases be a good estimation of the real variation of $[H_2]$ in time.

The presence of recombiners in a compartment, in addition to the thermodynamic condition of the mixture in the volume, also influences the mass (and energy) exchanges with the neighboring compartments. Thus a correct simulation can only be given by implementing a complete mathematical model of the recombiner in a containment code.

In HOCRA mode-2, the difference between the inlet and outlet flow rates is derived from the transients of pressure, temperature, and molar fractions of hydrogen and steam calculated by other codes in the absence of recombiners. This means that the balance between the entering and exiting flows into and from the volume are assumed the same both in the absence and presence of recombiners, or that the thermal-hydraulic exchanges between neighboring compartments are assumed to be not affected by the variations in energy and chemical composition due to the recombination. This hypothesis is actually untrue but is useful for an initial evaluation of the capability of the recombiners. The difference between the inlet and outlet flow rates is considered to be constant between two successive points of sampling, thus some peaks might be smoothed out in this time interval, if sampling is not sufficiently frequent.

Table 1 - Accident conditions

CASE	HYD1	HYD2	HYD3
Sequence	2" Hot Leg Break	4" Hot Leg Break	Transient (loss of feedwater)
System ↓	Status		
Accumulators	Yes (1 on 2)	Yes (2 on 2)	Yes (2 on 2)
Core makeup tanks	Yes (2 on 2)	No	Yes (2 on 2)
ADS	Yes (stages 1, 2, 3)	No	Yes (stages 1, 2, 3)
IRWST in vessel injection	Yes (at vessel failure)	Yes (at vessel failure)	Yes (at vessel failure)
IRWST cavity injection	No	No	No
Main Feedwater	No	No	No
Startup Feedwater	No	No	No
PCS sprays	Yes	Yes	Yes
PRHR system	No	No	No
Discharge Compartments	SG1 and IRWST	SG1	IRWST (+ a small steam flow in the U.C. Dome)
H ₂ produced (kg)	630	689	710

3. Application of HOCRA to accidents in an advanced PWR

There are many severe accident sequences in which the hydrogen mitigation system may have to intervene. For an advanced PWR, Westinghouse selected three reference scenarios, named HYD1, HYD2 and HYD3, from the most probable accident sequences, according to the probabilistic risk analysis (PRA), to show that the deliberate ignition system they chose meets the requirements of 10 CFR 50.34(f) [11].

In a LOCA, the release of steam and hydrogen in the containment takes place in the compartment where the primary system failure is located and/or in the compartment where the automatic depressurization system (ADS) is discharging. The quantity of steam and H₂ released will be diluted in the free volume of the containment in proportion to the discharge velocity and buoyancy.

Table 1 summarizes the situation of the reactor in the three cases. It presents the initial cause and the operating conditions of the intervention systems that determine the accident evolution, along with the compartments where the discharge takes place and the overall mass of hydrogen produced, estimated by Westinghouse with the MAAP 4.0 computer code.

The zirconium present in the active core is estimated to be 14 380 kg. The metal-water reaction produces two hydrogen moles for each mole of oxidized zirconium. In accordance with the requirements specified in 10 CFR 50.34(f) (100% oxidation), all the accident scenarios are assumed to generate a quantity of hydrogen equal to at least

635.6 kg. In the MAAP 4.0 calculations in which the overall mass of hydrogen produced would have been less than the quantity required, the input data were artificially modified in order to force the code to increase the production of hydrogen. The contact surface Zr-H₂O and the parameter that controls the collapse of the core were varied at the same time so as to alter them the least possible, the only aim being to go over 635.6 kg.

The simulations with MAAP 4.0 of the three scenarios show that the hydrogen fraction in the containment increases rapidly once the discharge has begun (2-4 hours after the beginning of the accident). They also highlight that the molar fractions of the gases in the various compartments are very similar to each other except for in the IRWST in cases HYD1 and HYD3. In all cases, the pressure is uniform between the various compartments. The thermodynamic conditions in the containment are therefore able to promote a degree of mixing of the atmosphere which is sufficient to homogenize the hydrogen (and the steam) in most of the free volume, apart from in the IRWST, when the discharge takes place there.

The definition of a system of catalytic recombiners for the examined advanced PWR can then be developed in two stages:

- 1) Evaluate the feasibility and capability of a recombiner system to control the hydrogen in accordance with the requirements outlined in 10 CFR 50.34(f) in the whole containment apart from the IRWST. Since the free volume of the IRWST gives a small contribution (~ 1%) to the overall free volume (~ 48 650 m³) and the molar fraction of hydrogen in it is greater or equal to the average in the containment, it is reasonable to assume that this system of recombiners is conservatively sized by applying HOCRA mode-1 to the whole volume of the containment.
- 2) Identify a mitigation system that is suitable for controlling the hydrogen in the IRWST volume, with a local analysis carried out with HOCRA mode-2.

3.1 Calculations with HOCRA mode-1

HOCRA mode-1 has been applied, both for NIS and 'large' Siemens recombiners, at the releases of hydrogen foreseen in the accident scenarios HYD1, HYD2 and HYD3.

Figures 4-6 show the variations in time of the discharge rate, W_{H_2} , the overall recombination rate for the system of NIS or Siemens recombiners, R_{rN} or R_{rS} , and the "dry" volumetric concentration (i.e. evaluated only taking into account hydrogen and air) without, Cd_{0H_2} , and with the NIS or Siemens recombiners, Cd_{NH_2} or Cd_{SH_2} .

In addition, for an analysis of the sensitivity of the code to the release typology, HOCRA mode-1 was also applied to the transient assumed by EPRI for evaluating the capability of a NIS recombiner system [5] (Fig. 7):

- 50% of the release in the first 10 min after the beginning of the accident
- 25% of the release in the following 35 min
- 25% in the following 50 min

All the elaborations were made assuming that:

$V = 48\,652\text{ m}^3$, free volume of the containment

$P_{0a} = 101\,325\text{ Pa}$, partial pressure of the air at $T_0 = 298.15\text{ K}$

The air amount in a containment is practically invariable before venting, therefore the "dry" H₂ volumetric concentration only depends on the H₂ molar density.

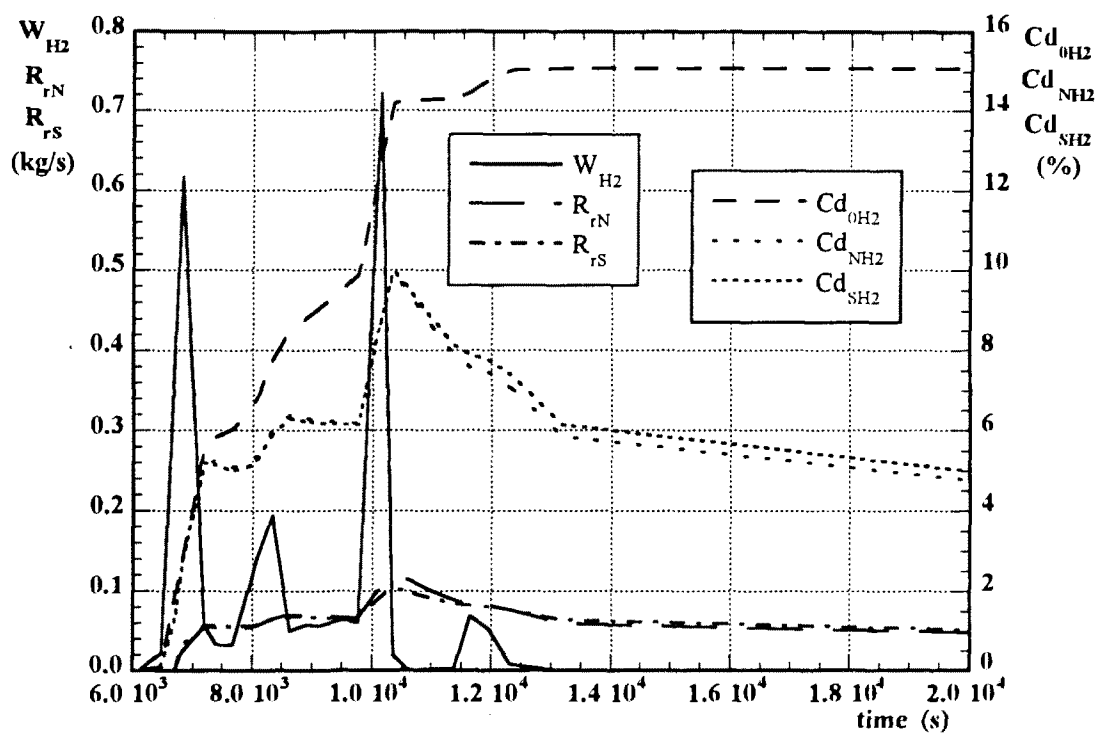


Figure 4. HYD1 scenario

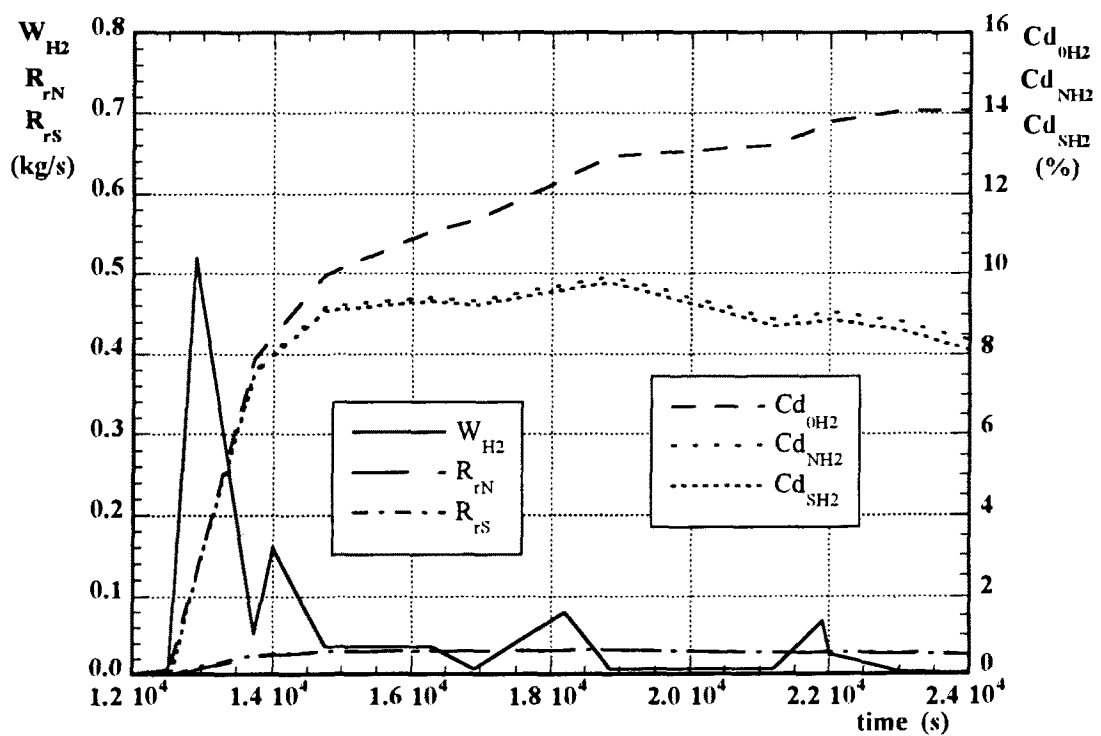


Figure 5. HYD2 scenario

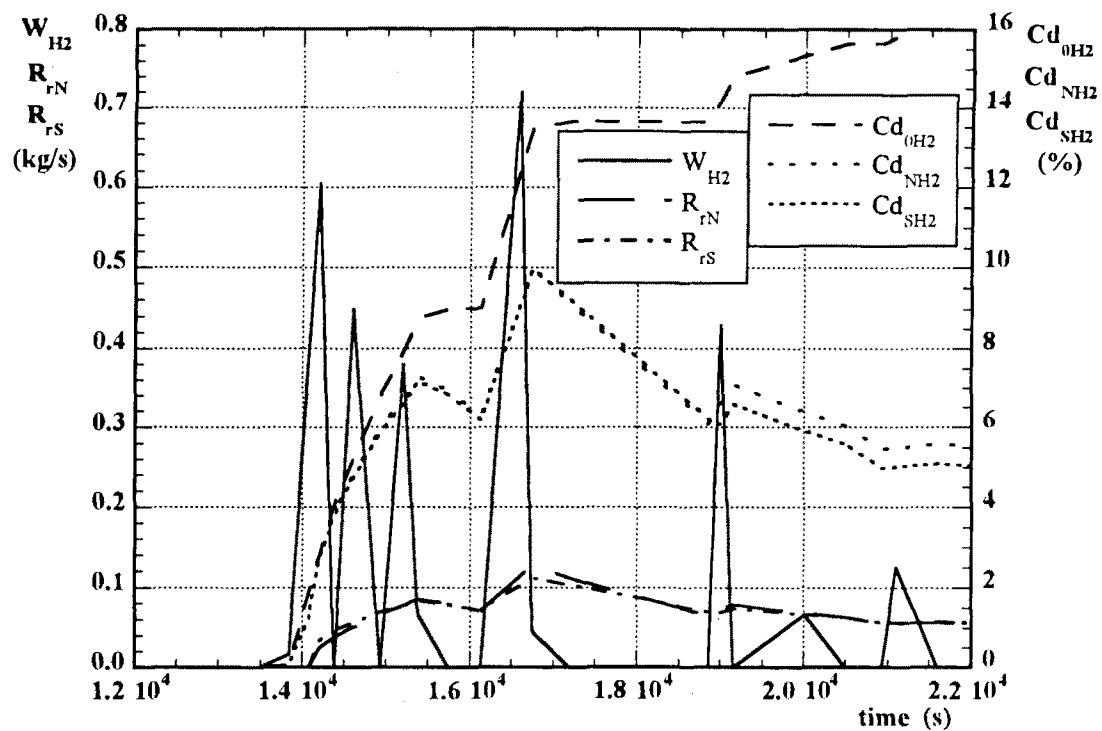


Figure 6. HYD3 scenario

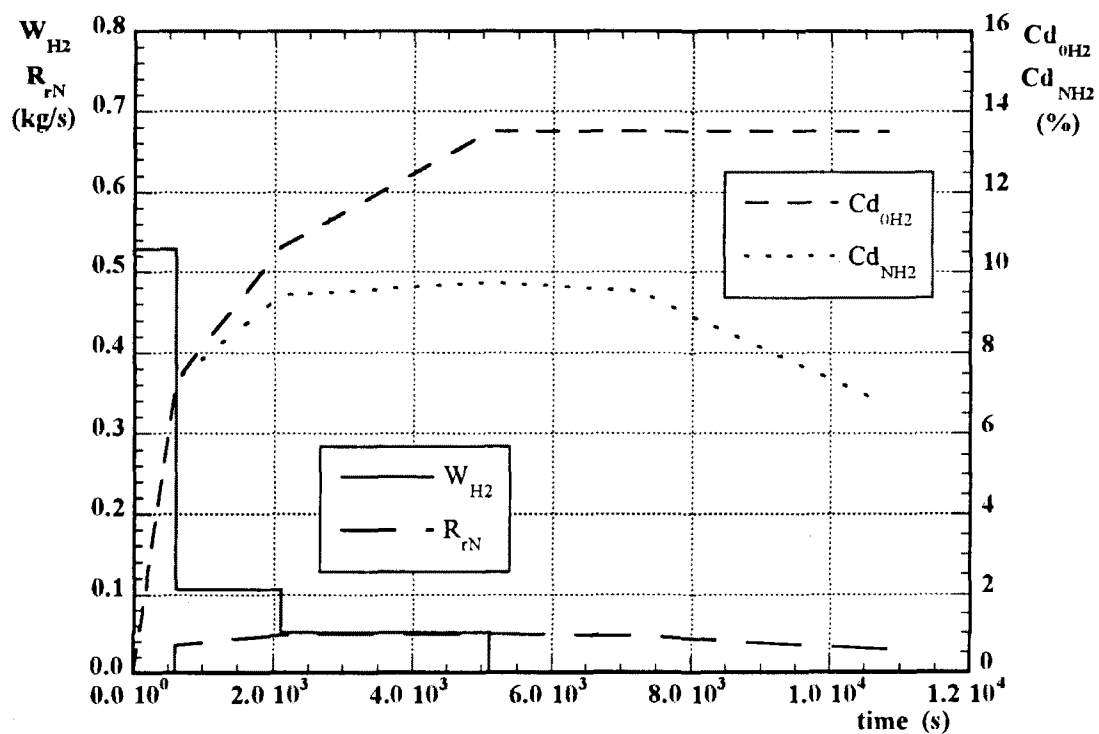


Figure 7. EPRI scenario

Table 2. Results of the analysis made with HOCRA mode-1 of the three accident scenarios

Accident	H ₂ released (kg)	NIS			Siemens "large"		
		Rec. No	Cd _{H2} max	Cd _{H2} (12h)	Rec. No	Cd _{H2} max	Cd _{H2} (12h)
HYD1	712.0	47	9.97 %	0.04 %	39	10.00 %	0.00 %
HYD2	660.5	12	9.92 %	1.54 %	12	9.78 %	0.53 %
HYD3	785.0	48	9.98 %	0.02 %	42	9.98 %	0.00 %

For both types of recombiner a start-up time of 600 s was imposed and fixed in all the executions. During this time it was assumed that the recombiner was not functioning, so as to simulate the response of a roughly first-order system with the response of a zero-order system with dead time.

The data on the release rate were derived from the graphics supplied for the various accident scenarios calculated with MAAP 4.0 [11]. In this operation a sufficient number of data was sampled to reproduce the release typology and to obtain an overall mass of hydrogen that was greater than the minimum indicated in accordance with 10 CFR 50.34.

HOCRA searches for the minimum number of recombiners needed to maintain the "dry" volumetric hydrogen concentration below a given limit throughout the whole transient. Limiting the "dry" volumetric hydrogen concentration is important because the maximum theoretical static overpressure due to a deflagration is proportional to the H₂ molar density. In addition, the likelihood of detonation in a hypostoichiometric mixture increases with the H₂ volumetric concentration, which is conservatively estimated with the "dry" value because this is independent of steam and thus the containment temperature. The chosen limit was 10% in accordance with 10 CFR 50.34 f.2.ix.

Table 2 summarizes, for each type of accident and recombiner, the total mass of hydrogen calculated by the code, the number of recombiners determined on the basis of the criterion mentioned above, the maximum volumetric concentration of hydrogen and the one after 12 h from the beginning of the release.

The results confirm that the number of recombiners calculated is essentially a consequence not so much of the total mass to "burn" but of the release rate when Cd_{H2} is near to the limit value imposed. A strong initial discharge of hydrogen (when the concentration is still below the limit imposed for the sizing), which is followed by a modest release rate, such as in HYD2, requires a lower number of recombiners than a hydrogen release that evolves in an inverse manner, as in HYD1 and HYD3.

The transient assumed by EPRI, Fig. 7, is very close to case HYD2: in the simulation with the NIS recombiner system, the code finds that 19 recombiners are needed to maintain Cd_{H2} < 10% in vol., which is in very good agreement with the 20 indicated in [5].

3.2 Calculations with HOCRA mode-2

HOCRA mode-2 was applied to the SG1, SG2 and IRWST compartments in the HYD1 accident scenarios.

The input data of HOCRA mode-2, concerning the transients of pressure, temperature, and molar fractions of hydrogen and steam for the various compartments, were derived from the graphics produced by the calculations made with MAAP 4.0 without recombiners [11].

Two executions were carried out for each compartment examined, in one the NIS recombiner was used, in the other the 'large' Siemens recombiner. The hydrogen transients calculated by the code do not show much difference between the two series of cases. Furthermore, the results for the SG1 and SG2 compartments are similar since there is a considerable affinity of accident evolution in the two volumes.

For the SG1 (free volume ~ 1160 m³) and SG2 (free volume ~ 950 m³) compartments, the results of HOCRA mode-2 show that only one recombiner for each volume would be able to maintain the "dry" volumetric concentration of hydrogen lower than 10%, with a peak value of around 9%. Leaving aside the structural differences of HOCRA modes -1 and -2 which make questionable any comparison of the results obtained from the two calculations, this result is in agreement with what was determined with HOCRA mode-1, that is 1 recombiner for about every 1000 m³ (1 in 1000 m³ for the NIS model, 1 in 1250 m³ for the large Siemens).

The analysis of the SG1 and SG2 compartments carried out with HOCRA mode-2 should rather be utilized for a comparison with what was obtained with the HOCRA mode-2 for IRWST.

In IRWST, the "dry" volumetric concentration of hydrogen increases suddenly even in the first instants following the beginning of the H₂ release and reaches values of around 20% before the recombiners come into action after the start-up time. Obviously, in these conditions no recombination system is able to maintain $C_{dH_2} < 10\%$ for all the transient. The release rate peaks (both the initial one described above, and the later ones) are so high that, even if a null start-up time is assumed, the code cannot determine a realistic number of recombiners that can control the rise in concentration.

The analysis conducted with two recombiners in the IRWST shows that the concentration of hydrogen is maintained practically at zero for all the time outside the release peak intervals, during which the system is actually not able to counter the increase in "dry" concentration which goes above 10%.

Although the conservativeness of "dry" evaluations should be taken into account, the comparison with the results obtained for the SG1 and SG2 compartments highlights the difficulties that the system of recombiners would have in controlling the hydrogen concentration in the IRWST in some accidents.

4. Conclusions

The optimum number and location of catalytic recombiners in a nuclear reactor containment that are needed to cope with the hydrogen generated during an accident, depend on the H_2 release rate (either in relation to the accident being examined or assumed by possible safety rules) and on the prediction of distribution of the gas mixture chemical composition by computer codes much more than it is for a system of ignitors. The capacity to predict H_2 generation and distribution is increasing, but there will inevitably be some limits to the level of detail. Uncertainties will always be linked to the degree to which the data obtained in scale model containments can be applied to full size reactor containments.

If recombining is the only mitigation measure for hydrogen, the number of recombiners to be installed in a nuclear reactor containment has to balance the H_2 release rate when the "dry" H_2 concentration reaches the maximum "acceptable" value. The number of recombiners calculated could be very different if this balance has to be satisfied in the whole containment or even locally, in every compartment. In this last case, a feasible number of recombiners could be used, if ignitors are provided in the most critical compartments. This number could be increased, to take into account any uncertainty in predicting H_2 generation and mixing as well as recombiner capacity. Additional mitigation measures, such as deliberate ignition or post-dilution, for scenarios with high hydrogen release rates, might be indispensable for controlling hydrogen.

Specifically, some first attempt calculations were carried out to verify the possibility of using a system of passive recombiners to control the hydrogen in an advanced PWR. The results were encouraging, though also ignitors seem to be needed to control the hydrogen in the IRWST.

Research will be needed to define better in theoretical and semi-empirical terms the behavior and the effects of the passive catalytic recombiners located in large volumes in post-accident thermal-hydraulic conditions. In addition, the containment codes will have to be directly implemented with recombiner models in order to better ascertain the number and location of the recombiners.

The regulatory requirements about hydrogen in severe accidents should be reviewed as well, to adapt them to new mitigation measures. In fact, at present, safety rules provide the maximum amount of H_2 that can be generated (e.g. equal to the H_2 generated by the oxidation of 100% of the total zirconium inventory, according to 10 CFR 50.34 f.2.ix) and the maximum acceptable H_2 molar density (e.g. $Cd_{H_2} = 10\%$, according to 10 CFR 50.34 f.2.ix). This may be enough to design pre-inerting and deliberate ignition systems, but the most important parameter to determine the number and location of the catalytic recombiners or injection nozzles of a H_2 dilution system is the H_2 generation rate, which, for example, 10 CFR 50.44 c.1+d.1+d.2 only provides for the Design Basis Accidents in connection to the use of the old types of active recombiners (a time period of 2 minutes is the interval after a postulated LOCA over which the oxidation of 5% of the total zirconium inventory should occur).

Nomenclature

ADS:	Automatic Depressurization System
Cd =	"dry" volumetric concentration
IRWST:	In-containment Refueling Water Storage Tank
J =	molar flow rate (mol/s)
LOCA:	Loss Of Coolant Accident
M =	molecular weight (kg/mol)
n =	mole number
N =	recombiner number
NVI =	compartment number
P =	pressure (Pa)
PCS:	Passive containment Cooling System
PRHR:	Passive Residual Heat Removal system
PWR:	Pressurized Water Reactor
R =	Correlation Coefficient
R _r =	recombination rate, that is the hydrogen mass recombined in the unit of time by a recombiner (kg/h or kg/s)
SG1/2:	compartments where a steam generator is located
t =	time (s)
T =	temperature (K)
U.C.:	Upper Containment compartments
V =	volume (m ³)
W =	discharge rate (kg/s)
[X] =	concentration, or molar density, of the chemical species X (mol/m ³)
Φ =	fuel equivalence ratio = $\frac{n_{H_2}/n_{O_2}}{(n_{H_2}/n_{O_2})_{stoich}} = \frac{1}{2} \frac{[H_2]}{0.21 [air]_0};$
when the molar density of the air is invariable (in Fig. 3 it is equal to the STP gas molar density, [air] ₀ = 44.64 mol/m ³), Φ only depends on the hydrogen molar density.	

Indices

0 =	initial conditions or without recombiners
a =	air
i =	inlet
N =	related to the NIS recombiners
o =	outlet
H ₂ =	related to hydrogen
O ₂ =	related to oxygen
r =	related to a recombiner
stoich:	stoichiometric
S =	related to the Siemens large recombiners
t =	total (conditions refer to all the containment)

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