

SPRAY DRYING WITH PLASMA-HEATED WATER VAPOUR

A  
THESIS

BY

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### ABSTRACT

Conventional spray drying has become a mature technology and the time has perhaps come to look at radical changes aimed principally at reducing capital cost (smaller chamber size, streamlining of flow patterns) and operating costs (better thermal efficiency and heat recovery from effluent streams). Following publication of a techno-economic study in which the feasibility of using a plasma of superheated water vapour as the heat source for a spray dryer was discussed, an experimental investigation of this proposed approach was carried out in a heavily insulated stainless steel drying chamber, 30.5 cm in diameter and 178 cm in height, surmounted by a DC jet plasma torch (nominal power: 40 kW), operated with both nitrogen and water vapour from a boiler. Several pressure atomizers, with different drop size distributions, were used and the feed consisted in turn of water and aqueous solutions of sodium nitrate, from 10% to 30% in concentration.

No drying gas was added to the system, and the drying medium consisted, therefore, of the small quantity of high temperature (circa 4 000 K) dissociated water vapour from the plasma torch amounting to about 8% by mass of the water in the

feed, and of the vapour produced by evaporation of the latter during drying. Because the volumetric flow rate of the vapour phase was small, the throughput per unit volume of drying chamber was quite high. For a given pressure atomizer and power fed to the plasma torch, an optimum range of feed rates was found, above which the salt was incompletely dried and below which degradation of the salt occurred.

These experimental results are preliminary but they indicate that water vapour plasmas can be used very effectively for the drying of non heat-sensitive materials, with appreciable savings in capital and operating cost. The latter is particularly attractive since the gaseous effluent from the system consists entirely of superheated steam, which can be easily recovered and utilized.



## RESUME

Le séchage par atomisation conventionnel est maintenant une technologie bien au point et le moment est peut-être venu de considérer des changements radicaux en vue de réduire les coûts d'installation (chambre plus petite, meilleure aérodynamique) et les coûts d'opération (meilleure efficacité thermique et récupération de la chaleur des gaz effluents). Une première étude technico-économique avait discuté la possibilité d'utiliser un plasma de vapeur d'eau surchauffée comme source de chaleur directe dans une chambre de séchage par atomisation. Faisant suite à cette étude, ce travail expérimental a été mené à bien dans une chambre de séchage en acier inoxydable parfaitement bien calorifugée. Elle faisait 30.5 cm de diamètre par 178 cm de hauteur et était surmontée d'une torche à plasma DC de puissance nominale 40 kW; Cette torche marchait avec un mélange d'azote et de vapeur d'eau légèrement surchauffée provenant d'une bouilloire. Plusieurs atomiseurs à pression ont été utilisés, donnant différentes distributions de taille de gouttes. L'alimentation a consisté tour à tour d'eau et de solutions aqueuses de nitrate de sodium dont la concentration variait de 10% à 30%.

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Aucun autre gaz n'ayant été rajouté dans la chambre, le milieu de séchage était donc composé de la faible quantité de vapeur d'eau (près de 8% en poids de la capacité d'évaporation) dissociée à très haute température (environ 4 000 K) provenant de la torche à plasma, et de la vapeur d'eau produite par évaporation lors du séchage. Du fait du faible volume de cette vapeur, la production par unité de volume de la chambre a été très élevée. Pour un atomiseur et une puissance de la torche à plasma donnés, une gamme de débits d'alimentation optimum a été déterminée, au dessus de laquelle le sel n'était pas entièrement séché et au dessous de laquelle il était partiellement fondu.

Ces résultats expérimentaux ont un caractère préliminaire mais ils indiquent que les plasmas à vapeur d'eau peuvent être très bien utilisés pour le séchage de matériaux peu sensibles à la chaleur, entraînant ainsi des économies appréciables dans les coûts d'investissement et d'opération. Ceci est en effet particulièrement séduisant puisque le gaz effluent à la sortie de la chambre est composé uniquement de vapeur d'eau surchauffée dont l'énergie peut être facilement récupérée et réutilisée.

### ACKNOWLEDGEMENTS

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GENERAL INTRODUCTION

## GENERAL INTRODUCTION

Spray drying may be defined as the operation whereby either a solution, a slurry, a suspension or a paste can be converted into a dried particulate form by spraying the feed into a hot gaseous medium. It is a single step, continuous drying process involving four basic stages: the atomization of the feed solution into a spray of droplets, the contacting between the spray and the drying gas, the moisture evaporation process (which is accompanied by crystallization of the solid), and the separation of the dry product from the drying gas.

Spray drying experienced its major development during the Second World War as a result of the increasing demand for dehydrated food products such as powdered milk, eggs and potatoes, as well as certain pharmaceutical or medical products such as blood plasma and amino acids. Since then, its range of applications has been enormously widened to include the production of coffee, tea,



detergents, pesticides, fertilizers, pharmaceutical products, ceramics, dyestuffs, some plastics and resins, other foodstuffs and recently the transformation of mineral ore concentrates into powders. Such a variety of products illustrates the ability of the spray-drying technology to meet a wide range of specific and exacting product requirements such as particle-size distribution, bulk density, residual moisture content, appearance and structure, solubility, colour, aroma and taste. But this has also led to a great number of different spray dryer designs and arrangements. It is not surprising then that spray drying has been the object of intensive research for a number of years, and an impressive number of experimental studies can be found in the literature. Some of these have indeed constituted excellent contributions to the understanding of specific aspects of spray drying such as atomization or drying mechanisms and the establishment of heat and mass transfer correlations, but have also been of little help for the specification and design of industrial spray dryers. One must also keep in mind that a large amount of empirical data has remained unpublished, since they are the property of the companies which are competitively designing and constructing spray drying process plants. This explains Masters' statement in his book (1979): "To a certain extent, spray dryers' design is still an art highly dependant on pilot-plant test data and existing industrial experience and, although theoretical design procedures exist, they

are based on many simplifying assumptions." This difficulty in designing spray dryers is principally due to the inability to describe the complex gas and droplets flow patterns induced by turbulence, recirculation and mixing that occurs in the drying chamber.

Despite all these difficulties, considerable improvements in spray drying technology have nevertheless been made to increase the overall thermal efficiency of the whole system and thus to reduce the operating costs of spray drying. Typical of these are:

- improvement in the chamber insulation
- increase of the drying gas inlet temperature
- decrease of the drying gas outlet temperature  
which has resulted in the development of two-stage drying
- partial recycling of the exhaust gas at a high outlet temperature which has resulted in the development of semi-closed-cycle spray dryers
- the more recent development of "self-inertizing" spray dryers (Christiansen, 1978)

All these developments have been reviewed in the "Spray Drying Handbook" by Masters (1979) and are summarized in Table I (Masters, 1980). However, none of these advances has overcome the major disadvantage of spray drying, namely, the large

TABLE I  
SPRAY DRYING DEVELOPMENTS

| LAYOUT                        | DRYING MEDIUM/FEED                                                                  | HEATING                          | APPLICATION                                                                                                                         |
|-------------------------------|-------------------------------------------------------------------------------------|----------------------------------|-------------------------------------------------------------------------------------------------------------------------------------|
| Open Cycle                    | Air/Aqueous                                                                         | Direct or Indirect               | General Layout, Exhaust Air to Atmosphere                                                                                           |
| Closed Cycle                  | Inert Gas/Nonaqueous (Organic Solvents)                                             | Indirect (Liquid Phase or Steam) | Developed for Evaporation, Recovery of Solvents; Prevention of Vapour Emissions, Elimination of Explosion, Fire Risks               |
| Semi-Closed (Partial Recycle) | Air/Aqueous                                                                         | Direct or Indirect               | Old Idea Now Developed for Improved Heat Economy                                                                                    |
| Semi-Closed (Standard)        | Air/Aqueous                                                                         | Indirect                         | Developed for Handling Active or Odorous Materials that Cannot Contact Products of Combustion; Elimination of Atmospheric Emissions |
| Semi-Closed (Self-Inertizing) | Air with low O <sub>2</sub> Content/Aqueous                                         | Direct                           | Developed for Handling Products that Exhibit Powder Explosion Characteristics; Elimination of Powder and Odor Emissions             |
| Two Stage                     | Any of the Above Layouts Plus Fluid Bed, Agglomerators, Flash Dryers as After Stage |                                  | Developed for Improved Powder Properties; Improved Heat Economy                                                                     |

volumetric flow rate of drying air needed to achieve the required degree of moisture removal, and the resulting intensive capital and operating costs for the whole spray drying plant, including gas heating and gas-product separation costs.

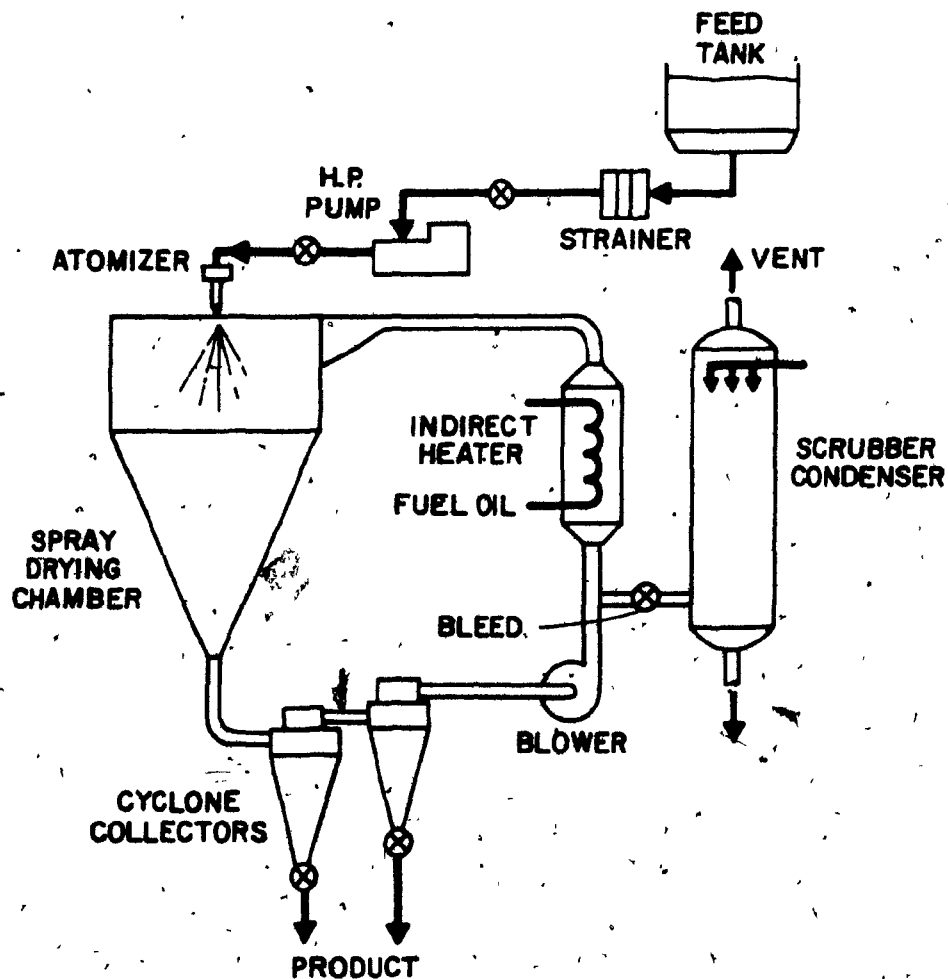
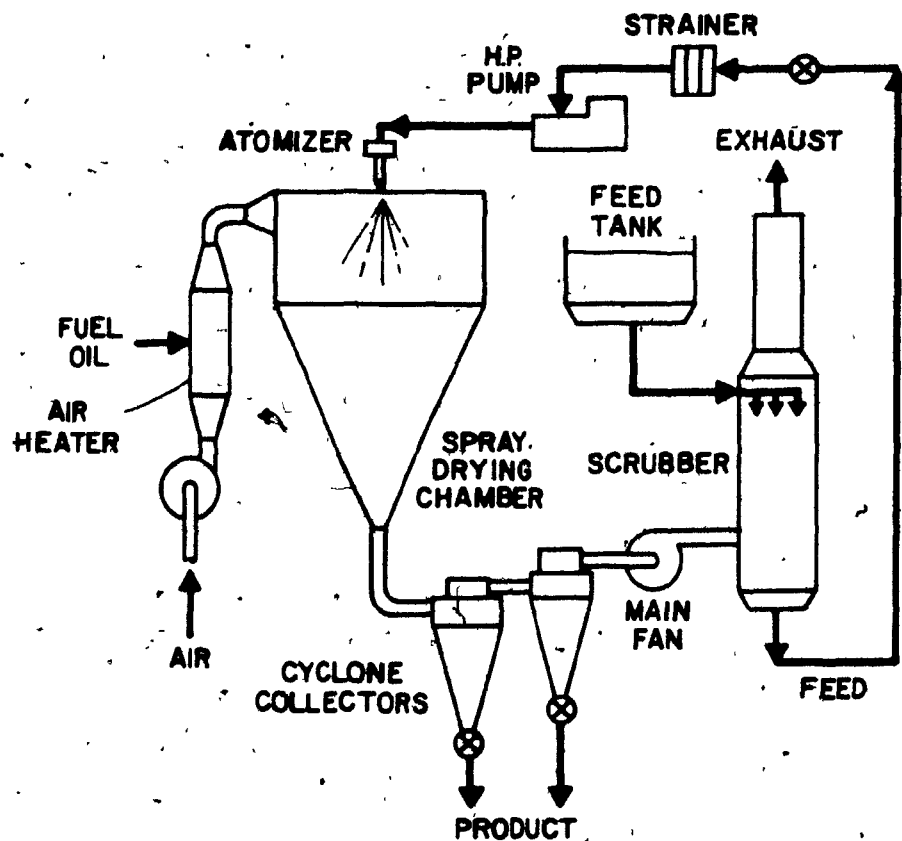
### The New Approach

In order to reduce these costs, a completely different approach was undertaken by Gauvin and Costin (1980) when they proposed the use of superheated steam as the drying medium. They compared a conventional open spray drying system operated with air as the drying medium (Figure 1-a) with a closed system in which superheated steam was the drying medium (Figure 1-b). The simulation was based on a computer program developed in 1976 by Gauvin and Katta. This program predicts the size of the drying chamber required for a given evaporative load and for given product specifications. The economics of both systems were also analyzed and their conclusion was that the use of superheated steam should result ~~in a~~ much smaller spray drying chamber, with about 21.5% lower annual operating costs under otherwise comparable conditions, for an installation of 2 300 kg/h water evaporative capacity. Savings of 33% in capital costs were also claimed but these were probably over estimated since a closed system needs fairly sophisticated equipment for gas tightness.

These promising conclusions led to the proposal of a

FIGURE 1

- a. OPEN CYCLE SPRAY DRYING SYSTEM -  
AIR AS THE DRYING MEDIUM  
(top)
- b. CLOSED CYCLE SPRAY DRYING SYSTEM -  
STEAM AS THE DRYING MEDIUM  
(bottom)



considerably more radical approach (Gauvin 1981) inspired by a technology developed in the early sixties in the laboratories of the Pulp and Paper Research Institute of Canada, and of McGill University, by Gauvin and several of his students and colleagues, called the "Atomized Suspension Technique" (AST). It was initially developed for the chemical recovery from spent pulping liquors and then successfully applied to several chemical and metallurgical processes. A good review on the AST development and its applications can be found in a paper by Gauvin and Gravel (1962). In this technique, the feed solution or slurry is sprayed at the top of a reactor, the walls of which are maintained at relatively high temperatures (700°C to 1 000°C), by means of hot combustion gases circulating in a jacket around the reactor. The fine droplets generated by the atomizer quickly decelerate from their initial high velocity until they approach their terminal velocity. Throughout this trajectory, called the nozzle zone, the droplets undergo intense evaporation, while extensive entrainment enters the expanding spray. When the relative velocity of the droplets becomes quite small, they now enter the so-called fall zone or free-entrainment zone. In the latter, they are simply conveyed by their own vapour and successively undergo evaporation and drying under the driving force of the heat transferred by radiation and convection from the hot walls. As the last traces of moisture are evaporated, the temperature of the particles increases sharply and pyrolysis

may result. Alternatively, gaseous reactants may be introduced at that point to effect oxidation, reduction, nitration, etc.; as desired. Obviously, the reactor can be used as a spray drying chamber only, if no pyrolysis is allowed to occur, or if no secondary gas stream is introduced in the lower section of the vessel to initiate a chemical reaction. It is important to realize that the drying medium in this case consists entirely of the superheated steam generated during the evaporation and drying of the aqueous solution initially contained in the droplets. The entire process results in a much smaller reactor size and an easier recovery of the latent heat of the exit drying vapour. However, the system presents two important shortcomings: (a) the low rate of heat transfer from the hot wall to the suspension of drying droplets, which results in a low production rate; (b) corrosion of the hot walls in contact with certain reaction products, such as sodium carbonate, sodium sulphate, etc.

These two limitations severely restricted the wide-scale utilization of this technique, although a number of industrial applications have been found for it.

In the meantime, the use of electric arcs to generate partially ionized gases - called plasmas - at very high temperatures was being developed. Industrial-scale plasma generators are now available, which are finding applications principally in the



steel industry. Argon, air, hydrogen and nitrogen can now be routinely heated by means of plasma devices of various types. Because of the aggressive nature of dissociated water vapour, however, only Alusuisse has developed a plasma device for the production of a steam plasma heavily contaminated, however, with carbon from the cathode. The possibility of eventually generating a plasma of pure steam is sufficiently promising, however, to have prompted the publication of the previously mentioned proposal (Gauvin, 1981) in which a superheated steam plasma at a very high temperature would provide a direct and efficient heat source for drying. A steam plasma has many advantages which will be reviewed in a subsequent chapter, and it has also the advantage of maintaining the evaporation and drying processes in a pure superheated steam medium. The analysis was based on the design theory proposed by Gauvin and Katta (1976), simplified, however, for a two-dimensional situation, since no vortex flow is involved in the new approach. In addition, this program had to be modified to account for the properties of steam plasma and, although a great number of assumptions were made without any experimental support, it showed clearly many advantages of the new system such as a large reduction in chamber size, the possibility of operating under pressure (which is impossible with conventional spray-drying), and the absence of blowers and compressors. Capital costs are difficult to compare, but large reductions in operating costs can

be effected if use can be made of the superheated steam produced in large quantities by the process.

The major shortcoming of this system is that it is applicable only to the drying of non-heat-sensitive materials, because of the high temperatures of the drying steam in the whole chamber.

The objective of this thesis was to test the technical feasibility of this novel approach to spray drying, to identify possible areas of difficulty in its industrial applications, and to investigate the validity of some of the assumptions made in the 1981 analysis.

The presentation of the experimental evidence will be preceded by a brief literature review of the aspects of spray drying theory and plasma technology which are most pertinent to the research project.

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L I T E R A T U R E   R E V I E W

## SPRAY DRYING

### INTRODUCTION

Intensive research work has been performed for the past several years to investigate the various mechanisms of liquid atomization and to study the complex transport phenomena involved in the contacting of the cloud of drying droplets with the surrounding hot gas. With a better understanding of these two distinguishing features of spray drying, modelling of the whole operation has received increasing attention. An extensive review of the literature dealing with many aspects of spray-drying up to 1967 can be found in Baltas' thesis (1967).

Only two aspects of the vast body of evidence will be treated here because of their relevance to this thesis: the first deals with the atomization process and the spray characteristics, while the second is concerned with the evaporation of liquid droplets. A small section on the modelling of spray drying has also been inserted between these two parts, and it briefly describes and criticizes several sophisticated models which have appeared very recently in the literature.

## ATOMIZATION

### Types of Atomizers

Atomization is indeed a crucial part of the spray-drying process since it governs the size and quality of the dried product. The function of the atomizer is twofold: to subdivide the liquid feed into droplets of the required size distribution and to promote the dispersion and mixing of these droplets with the surrounding drying gas. The initial drop size distribution, the mean droplet diameter and the largest droplet diameter are the important characteristics of the atomized spray, and they are essentially determined by the design and operation of the nozzle, by the liquid properties and, to a lesser extent, by the properties of the drying medium (especially its density).

The selection and operation of the atomizer is particularly important since the initial generated drop size distribution determines not only the size distribution of the product, its homogeneity and its resulting characteristics, but also the evaporation and drying rates and, consequently, the design of the dryer. There are four basic types of atomizers, depending on the form of energy which is used to break up the bulk liquid: centrifugal, pressure, kinetic or sonic energy.

- ° Centrifugal energy is used in rotary atomizers which can be either vaned wheels or vaneless discs. These are well suited to handle high feed rates in large commercial spray dryers for the production of fine to medium-coarse powders (30-120  $\mu\text{m}$ ) of high homogeneity. Besides\*they are particularly recommended for slurries, sludges, high-viscosity fluids such as thixotropic materials or thermoplastics, and corrosive or abrasive materials. They also present a great flexibility of operation.
- ° Pressure nozzles can produce both coarse or fine sprays of high homogeneity, depending on the pressure involved. Modern designs incorporate spiralling channels which impart a vortex motion to the issuing sheet of liquid. However, they do not present a large flexibility of operation since, for a given nozzle, both the feed flow rate and the spray characteristics are controlled by the atomizing pressure. However, this lack of flexibility has been greatly improved by the creation of interchangeable swirl spirals and orifices, and these nozzles are extensively used in many industries for low viscosity feeds containing no solid particles in suspension. They are also well suited to the operation of small pilot-scale spray dryers.



- ° Two fluid (or pneumatic) nozzles use the kinetic energy of a high-velocity gas which is impacting on the liquid bulk to disintegrate it. Generally, the air stream is rotated within the nozzle to provide a better energy transfer. At a constant feed rate, different degrees of atomization can be achieved by adjusting the gas pressure and hence its flow rate, and they are well suited to laboratory and pilot-scale equipment. However, their use is considerably limited by the high cost of the compressed gas, the difficulty of scaling-up the results, and also by the considerable reduction of the spray dryer capacity because of the large amount of secondary gas entering the chamber.
- ° Sonic nozzles are only now being developed but they should eventually permit the atomization of products which cannot yet be atomized, such as non-Newtonian fluids.

A pressure nozzle was found to be the most suitable for the present work, because of the small diameter of the drying chamber and because no dilution by secondary gas (such as in a pneumatic nozzle) could be tolerated. For this reason, only the characteristic features of pressure nozzles will be discussed

in the following section.

### Characteristic Features of Pressure Nozzles

The most commonly used pressure atomizer for small flow rates of liquid is the swirl (or centrifugal) pressure nozzle. It consists basically of a swirl chamber into which the liquid enters tangentially through several orifices and from which it leaves through a central orifice. The liquid swirls in the chamber around an air core and is discharged from the orifice in the form of a hollow cone. If the formation of the air core at the centre is avoided, a solid-cone pattern results, the droplets being distributed almost uniformly throughout the whole spray. This solid-cone pattern is suitable for countercurrent spray dryers whereas the hollow-cone pattern is more commonly used in cocurrent chambers. Another widely used pressure atomizer is the single hole fan spray nozzle where jets of liquid produced from two opposing orifices impinge on each other.

Two important characteristics of pressure nozzles are the remarkable symmetry of the spray-pattern reported by most of the investigators and particularly by Baltas (1967), and the very small spray-angle that can be achieved. Dombrowski and Wolfsohn (1972) studied the atomization of water using some forty-two standard commercial nozzles and reported a non-uniform distribution for the

smallest nozzles only, having flow numbers below 0.1 (for pressure nozzles, the flow number  $FN$  is defined by the ratio of the liquid volumetric flow rate to the square root of the atomizing pressure).

In these atomizers, both the liquid flow rate and the initial drop size distribution are functions of the liquid properties, atomizing pressure, nozzle orifice diameter and design. Hence, independent control of the feed rate and drop size is impossible and this inflexibility partially offsets the numerous advantages of this type of atomizer which have been mentioned above.

Maintenance is also a problem, since the performance of these nozzles is strongly affected by changes in the atomizing pressure due to erosion or clogging of the nozzle orifice.

#### Prediction of Pressure Nozzle Performance

The drop size distribution of a spray is represented by a function involving at least two parameters: a convenient mean diameter and a measure of the drop size dispersion. Depending on the applications, the chosen distribution function can be defined either by the percentage or the surface or volume fraction of the total spray, represented by the droplets lying in a given interval of diameters.

A spherical shape has to be assumed for the droplets prior to any use of these distribution functions.

### a. Prediction of the Mean Droplet Size

Two mean diameters are mostly used in spray drying studies:

- ° The median diameter  $\underline{D_m}$  above (or below) which 50% of the droplet cumulative size distribution lies.
- ° The Sauter mean diameter  $\underline{D_{VS}}$  (or sometimes  $\underline{D_{32}}$ ), useful for heat and mass transfer predictions since it gives the volume-to-surface ratio of the whole spray. This diameter is defined by:

$$D_{VS} = \frac{\int_{D_{min}}^{D_{MAX}} D^3 f(D) d(D)}{\int_{D_{min}}^{D_{MAX}} D^2 f(D) d(D)} \quad (1)$$

where  $\underline{f(D)}$  is the relative frequency distribution function of the droplet diameters.

Several correlations are available in the literature to predict the mean droplet size generated by different types of pressure atomizers. One of the most widely quoted relationships was established by Tate and Marshall (1953) who studied the spray characteristics in terms of the orifice diameter of the nozzle, the vertical velocity component (which is related to the atomizing pressure) and the tangential velocity component (related to the nozzle design) of the liquid jet. They found:

$$D_{VS} = 1.126 \times 10^4 (d_o + 4.32 \times 10^{-3}) \exp (3.96/V_v - 0.031V_t) \quad (2)$$

for the range  $0.33 \times 10^{-3} < d_o < 10^{-3} \text{ m}$

$$12 < V_v < 46 \text{ m/s}$$

$$0 < V_t < 15 \text{ m/s}$$

The vertical velocity component (in m/s) was

$$V_v = 0.0398 Q/d_o^2 \quad (3)$$

assuming that the orifice runs full, where

$Q$  is the nozzle capacity in  $\text{m}^3/\text{s}$

$d_o$  is the nozzle V orifice in m

The tangential velocity  $V_t$  (in m/s) was determined for grooved core nozzles only with the following expression:

$$V_t = 0.31 Q \cos \alpha / N_G A_G \quad (4)$$

where  $\alpha$  is the angle made by grooves with the horizontal

$N_G$  is the number of grooves

$A_G$  is the cross sectional area of a single groove

However, in swirl pressure nozzles, an air core exists to induce the swirl action and the cross sectional area of the orifice is not entirely available for the liquid flow as it is assumed in the above calculation of  $V_v$ . It was shown (Darnell cited by Tate and Marshall (1953) that the air core remains

relatively constant for a given nozzle and different operating conditions, which means that the actual vertical velocity is from 1.4 to 2.5 times larger than the one calculated above. Nelson and Stevens (1961) have correlated the experimental data of Darnell and other workers, using dimensional analysis. They showed that the cross sectional area available for liquid flow at the orifice is related to the diameter  $\underline{d} = \underline{d}_o (1 - \underline{d}_c / \underline{d}_o)$  where  $\underline{d}_c$  is the diameter of the air core and that  $\underline{d}_c / \underline{d}_o$  is a function of the maximum cone angle  $\theta_m$  of the spray only. A plot of  $\underline{d}_c / \underline{d}_o$  against  $\theta_m$  is given, based on the results of Darnell and others with different nozzles.

Tate and Marshall (1953) have noted also that an increase in liquid viscosity produces a sharp increase in the mean drop size and a decrease in spray uniformity. More recently, Dombrowski and Wolfsohn (1972), previously cited, showed that the spray drop size is related to the ejection velocity and atomized flow rate but is independent of the nozzle design. They developed a semi-empirical expression for the Sauter mean diameter:

$$\underline{D}_{VS} = 0.04 Q^{1/3} / p^{1/2} \quad (5)$$

with  $\pm 14\%$  standard error where

$\underline{D}_{VS}$  is in microns

$\underline{Q}$  is the volumetric liquid flow rate in  $m^3/s$

$\underline{p}$  is the atomizing gauge pressure in  $N/m^2$

These correlations are all semi-empirical and are thus difficult to use with much confidence for the prediction of droplet sizes in

industrial spray dryers. A better way of prediction, according to Perry's (1973), is to consider the manufacturer's data established for the given nozzle working with an air-water system and to correct them for the different liquid properties by the following equation:

$$D_m(\text{system}) / D_m(\text{water}) = (\sigma_{\text{system}}/0.073)^{0.5} (\mu_{\text{liquid}}/0.001)^{0.2} (1000/\rho_{\text{liquid}})^{0.5} \quad (6)$$

where  $\sigma_{\text{system}}$  is in N/m

$\mu_{\text{liquid}}$  is in poiseuille

$\rho_{\text{liquid}}$  is in kg/m<sup>3</sup>

#### b. Prediction of the Drop Size Distribution of the Spray

A great number of mathematical distribution functions have been used with varying success to represent the drop size characteristics of sprays: among them were the Rasin-Rammler equation, the log-normal and square-root-normal distributions, the Nukiyama-Tanasawa equation, the Kim and Marshall equation and the upper-limit function of Mugele and Evans. They were either used because they happened to fit the experimental data or developed on a more rational basis to improve on another prior function. Two of these have proved particularly useful to describe the spray characteristics of pressure nozzles (Nelson and Stevens, 1961): the upper-limit function of Mugele and Evans (1951) and the square-root-normal distribution function used by Tate and Marshall (1953) to fit their experimental data.

The square-root-normal distribution function is defined by:

$$d(V)/d(D) = (2S\sqrt{2\pi D})^{-1} \exp[-(\sqrt{D} - \sqrt{D_m})^2 / 2S^2] \quad (7)$$

where  $S$  is a measure of the size uniformity:

$$S = 0.78 (\sqrt{D_{84.13}} - \sqrt{D_m}) \quad (8)$$

where  $D_{84.13}$  is the drop diameter corresponding to 84.13% volume undersize on the cumulative distribution curve.

The upper limit function was introduced by Mugele and Evans (1951), in a paper which reviewed the mathematical expressions used for the representation of sprays, together with their limitations. It is a modified form of the log-normal law and contains a third additional parameter, the stable maximum droplet size, to emphasize the existence of a maximum drop size rather than to consider very low frequencies for large drops. The volume frequency curve is determined by:

$$d(V)/dy = \delta/\sqrt{\pi} \exp(-\delta^2 y^2) \quad (9)$$

where  $y = y(D) = \ln [aD / (D_{MAX} - D)] \quad (10)$

$a$  is a constant given by:

$$a = (D_{MAX} - D_m) / D_m \quad (11)$$



The stable largest droplet diameter  $D_{MAX}$  is obtained by a trial and error procedure to give the best straight line when  $D/(D_{MAX} - D)$  is plotted against the cumulative number or volume frequency undersize on a log-probability chart. A first and good estimate for  $D_{MAX}$  is given by :

$$D_{MAX}/D_m = (D_m(D_{90} + D_{10}) - 2D_{90} \times D_{10}) / (D_m^2 - D_{90} \times D_{10}) \quad (12)$$

where  $D_{10}$ ,  $D_{90}$  correspond respectively to the 10th and 90th percentile of the cumulative distribution. The size distribution parameter  $\delta$  is given by the slope of the straight line:

$$\delta = 0.394 / \log (u_{90}/u_{10}) \quad (13)$$

where  $u = D/(D_{MAX} - D)$  and  $u_{90}$ ,  $u_{10}$  correspond respectively to the 90th and 10th percentile of the cumulative distribution. The Sauter mean diameter is:

$$D_{VS} = D_{MAX} / [1 + a \exp (1/4 \delta^2)] \quad (14)$$

where  $a$  is the constant from Equation (11).

These distributions are still being further improved by using statistical methods to determine the best fits of the parameters. Very recently, Lekic, Bajramovic and Ford (1976) have developed a method to obtain the best fit for experimental droplet size distribution. In their paper, they reported that the experimental data are always analyzed by some linear representation

on a properly chosen chart and that they could better be approximated by using statistical distributions and determining the parameters by least square fittings. Using experimental data from several authors, they concluded that the upper limit function of Mugele and Evans and the  $X^2$ -distribution are the best approximations and that the normalized-volume distribution gives better results than the normalized-number distribution.

### c. Experimental Determination of Nozzle Performance

From the above, one can easily conclude that spray characteristics such as median, Sauter or largest diameters can be obtained with some confidence only by an experimental determination of the drop size distribution in the particular conditions and by an appropriate fitting of the data.

A very comprehensive review on drop size determination can be found in the thesis of Baltas (1967).

A more recent review on drop size measurements is presented in a paper by Azzopardi (1979). The main principles of each method are outlined, the range of sizes for which the method is suitable is given and particular attention is devoted to the difficulties and sources of error encountered in such a determination. The author concluded that no simple overall recommendation is possible for drop size measurements since the

optimum method depends on the type of distribution required and on the experimental conditions of the operation studied.

#### d. Aerodynamic Behaviour of Liquid Sprays

The drop formation process in pressure nozzles has been widely studied but less attention has been given to the behaviour of the resultant spray. The spray drying chamber below the atomizer can be divided into two regions, according to Gauvin and Katta (1975). The droplets have a very high velocity at the exit of the nozzle but, owing to their small size, they decelerate rapidly and reach their terminal velocity within a few feet from the nozzle. This region of deceleration is called the "nozzle zone" and its length has to be determined experimentally because of the high degree of turbulence resulting from the expansion of the jet. Intense back mixing of the surrounding gas exists also in that zone, owing to the high momentum transfer between the gas and the jet and the low pressure region created by the jet.

The next zone where the droplets have a constant velocity relative to the fluid is the "fall" or "free entrainment zone." The jet has been broken up at the entrance to that region and the two phases (spray of droplets and drying medium) are now well mixed.

The prediction of the flow patterns in both zones is of much importance not only for the calculation of the evaporation rate

of the droplets but also to ascertain the degree of impingement of the droplets on the confining walls. This impingement process is highly dependent on the spray angle of the nozzle and on the turbulence of the surrounding gas too, since ~~small~~ droplets - as those generated in spray drying - appear to follow the turbulent macro-eddies of the carrier gas (Friedlander and Johnstone, 1957).

The theory for 2-dimensional or 3-dimensional jets in the laminar and turbulent ranges has been studied by Schlichting (1968), based on turbulence and boundary layer concepts but the presence of a dispersed phase makes the problem much more complicated. In fact, very few theoretical or experimental works are dealing with the behaviour of liquid sprays in closed chambers. Venezian (1956) made an experimental study of the turbulence and entrainment characteristics of some atomizing pneumatic and pressure nozzles. It was essentially a qualitative work, since turbulence was only measured by visual observation. However, he was able to establish a correlation for the global entrainment rate of air within the spray, predicting that entrainment and turbulence for pressure nozzles are considerably lower than for pneumatic nozzles. A change in diameter of the chamber from 7-3/4 inches (ID) to 12 inches was reported to have no effect on flow patterns. Rothe and Block (1975) have presented an analysis of the effect of the entrained gas flow within the spray for a non-condensing gas

environment. As the liquid drops at the periphery of the spray are entrained with the gas towards the centre of the spray, the spray contracts. Their theory predicts that the spray contraction increases with increasing ambient pressure and nozzle pressure drop and decreases with increasing nozzle orifice diameter and mean droplet size. The effect of gravity on vertical sprays has been shown to be negligible.

Two other workers, Benatt and Eisenklam (1969) have derived a model to describe the entrainment of air into water sprays issuing from swirl nozzles under ambient conditions. It happens to correlate the experimental results quite well and requires the introduction of a dimensionless parameter of entrainment  $E$ , approximately constant for each nozzle, and from which the actual mass flow rate of gas entrainment can be calculated. Their treatment indicates that a similarity between sprays and gas jet exists, since for gas jet entrainment  $E$  is also a constant equal to 0.282.

#### MODELLING OF SPRAY DRYING SYSTEM

Over the last three decades considerable effort has been devoted to the development of models to describe the operating characteristics of spray dryers and to assist in their design. The early models were obviously very simple but they have been

useful in the further development of more sophisticated models, which was made possible when computers enabled the handling of stepwise complex iterative procedures.

The major difficulty still encountered when modelling a spray drying operation is to describe precisely the interaction of the spray droplets with the drying gas, especially near the atomizer. All three transfer mechanisms of mass, momentum and energy are involved in the spray/drying gas contacting process and they are completely interdependent so that simple analytic expressions cannot describe the variations of drying gas and droplet properties over the entire drying time. As a result, models so far developed are useful for one specific spray drying configuration only because of the simplifications and assumptions introduced and of the empirical correlations used, based on pilot plant test data or available industrial experience.

A very extensive review on spray-air contact modelling in spray-drying systems has been recently made by Crowe (1980). He classifies the existing models according to their geometry and to whether they consider a coupling between phases or not; that classification shows very well the progressively increasing complexity of the different models. Several of the more recent ones are discussed:



- ° The model of Parti and Palancz (1974) is severely criticized: it is one-dimensional, involving no radial expansion of the spray but it incorporates all the coupling phenomena between the two phases; however, according to Crowe, the equation describing droplet momentum is wrong for both cocurrent and countercurrent systems, thus invalidating the model.
- ° The model developed by Gauvin and Katta (1976) represents, according to Crowe, "an appealing approach that appears to capture the essence of the physical processes." It is a three-dimensional model, as far as the equations of motion are involved, consisting of a jet region (or nozzle range region) and of a free-entrainment annular region. No radial gradients are considered in the jet region, all properties of the drying gas and the droplets being uniform across each section. It becomes truly three-dimensional in the free-entrainment zone where it has the merits of using vortex motion theories recently developed in this laboratory by Bank (1975) and further refined by Reydon (1978). It involves

thermal coupling of the two phases, but neglects momentum coupling, except for the entrainment of drying gas within the spray. Its main shortcoming is the fact that parameters for the jet, such as centreline velocity and entrainment ratio, must be provided as input according to semi-empirical correlations which may not be available for all spray dryer configurations.

- ° Finally, Crowe presents his own model, the "Particle-Source-In-Cell" model (PSI-Cell) which is "the most general model applicable to any axisymmetric system and capable of including all coupling phenomena with a minimum of empirical input." The model is compared to the experimental data for Manning and Gauvin (1968), which are the most detailed data available on the evaporation of a water spray into a hot air stream. It gives effectively good predictions of the spray dryer operating characteristics and performance but it fails to predict well the amount of evaporation in the vicinity of the atomizer because of the lack of detailed empirical data in that region. Besides, it involves a fairly complicated procedure



requiring a large time and capacity of computer which may be rather costly.

Some other papers on spray drying modelling can be found in the very recent literature. Ade-John and Jeffreys (1978) have identified four zones of flow in a transparent counter-current spray dryer using tracer experiments and they have given correlations of zone volumes in the spray dryer. All their runs have been conducted at an air temperature of  $31^{\circ}\text{C}$  which is not quite realistic for industrial operation and flow characteristics are likely to be different at higher temperatures. Pham and Keey (1977) described the evaporation of a spray in the jet zone from a nozzle atomizer using "equivalent flat radial profiles" of velocity, temperature spray concentration and humidity. They found that evaporation is considerably reduced in the jet because of the limited amount of air entrained into the jet and of the high velocity of the droplets. Their conclusion is hardly justified since they have assumed that the driving force number for evaporation is almost zero ( $\sim 10^{-6}$ ) within the jet to perform all the calculations. In fact, this assumption is rather doubtful and is inconsistent with the conclusions of other workers (Katta and Gauvin, 1975) who have found that most of the evaporation process takes place in the nozzle zone. Moreover, they ignore completely the fraction of gas surrounding the spray which is not entrained within the spray.

Janda (1978) has developed a method to calculate the minimum required height of a drying tower operating with air at 150-250°C and with a single atomized substance (solution or suspension). He has tested several dryer configurations and his work is essentially useful for comparing these different arrangements since it is otherwise too simplified. No change of diameter occurs for the spherical droplets, the drying proceeds only in a period of constant drying rate, the second drying period being negligible for particles up to 1 000  $\mu\text{m}$ .

### EVAPORATION OF DROPLETS

#### Evaporation of Liquid Droplets in Air at Low Temperature

One can find in the literature a great number of correlations for pure heat transfer to a spherical drop by forced convection at low temperature. They are semi-empirical and in the form:

$$\text{Nu} = a' + b' (\text{Re})^p (\text{Pr})^q \quad (15)$$

The most quoted of these correlations is the Ranz and Marshall Equation (1952) determined on an air-water system valid for  $1 < \text{Re} < 450$ ,  $\text{Pr} < 250$  as pointed out by Hughmark (1967):

$$\text{Nu} = 2.0 + 0.6 \text{Re}^{1/2} \text{Pr}^{1/3} \quad (16)$$

In that equation, the thermodynamic and transport properties are evaluated at the film mean temperature and composition. A similar correlation is applicable for mass transfer for  $1 < Re < 450$  and  $S_c < 250$ :

$$Sh = 2.0 + 0.6 Re^{1/2} S_c^{1/3} \quad (17)$$

Most of these experimental equations have been obtained from the study of the evaporation rate of a single drop suspended in the hot gas stream. The drop was suspended by means of a glass capillary or a thin thermocouple and its variations of mass or size with time were recorded.

Much work has been performed in this laboratory on the evaporation rate of actual sprays in hot air for droplets falling at their terminal velocity (Dlouhy and Gauvin, 1960) or decelerating in the zone close to the atomizer (Manning and Gauvin, 1960). It was experimentally verified that the Ranz and Marshall Equation, derived for an isolated drop in air, was valid for describing the evaporation of a cloud of droplets concurrently entrained in a turbulent air stream, if the droplets are very small. In this case, according to the authors, the distance between the droplets is such that no interference occurs between their respective boundary layers and each droplet evaporates as if it were alone. Moreover, the contribution of natural convection is negligible for

such small droplets, as Pei and Gauvin (1963) pointed out.

Numerous theoretical analyses have also been made in the past and they have recently been reviewed by Beard (1970).

Drops containing solids differ in behaviour from pure liquid ones in two important ways:

- ° the temperature of the droplet increases as evaporation proceeds (except for colloidal suspensions)
- ° the presence of the solids slows down the evaporation rate, since, in most cases, a crust of solid is formed at the surface of the particle

Two periods of drying can then be distinguished:

- ° a constant rate period until the first solid appears
- ° a falling rate period which appears to be characteristic not only of the nature of the material but also of the conditions under which it is dried (Baltas, 1967)

### Evaporation of Liquid Droplets in a High Temperature Gas

At high temperatures, the evaporation rate is much higher and the effect of mass transfer on heat transfer of liquid droplets cannot be neglected anymore. Gauvin and Hoffman (1962) have shown the following important characteristics for high-temperature heat transfer to a cloud of droplets:

- The important radial mass efflux of vapour from the droplets affects the gas flow pattern and therefore influences the heat transfer rate.
- Radiant heat transfer may provide a large fraction of the total thermal energy required by the droplets and must be considered. According to the author, the vapour boundary layer around the droplet absorbs a negligible amount of the thermal radiation and therefore the droplet is assumed to be in a non-absorbing medium when calculating the total heat flux from the gas to the liquid.
- The sensible heat content of the evolved vapour cannot be neglected when compared to the latent heat of vaporization. Furthermore, the Nusselt number for heat transfer must be corrected in the following way:

A true Nusselt number  $Nu_T$  is defined for pure heat transfer in the absence of evaporation and is given by the Ranz and Marshall Equation.

An actual Nusselt number  $Nu_A$  yields the actual rate of heat transfer available for evaporation at the surface and is related to the true Nusselt number by the approximate equation:

$$Nu_T = Nu_A [B' / \ln(1 + B')] \quad (18)$$

where  $B'$  is the modified Spalding number:

$$B' = C_p \Delta T / [\lambda - q_r / \dot{m}] \quad (19)$$

The variations of the physical properties are taken into account by using the values calculated at the arithmetic mean film temperature across the boundary layer surrounding the droplet.

Many other correlations have been proposed, based on the Spalding number,  $B$ , which was not modified to account for radiation:

$$B = C_p \Delta T / \lambda \quad (20)$$

Pei and Gauvin (1963) point out that  $B$  and  $B'$  do not actually differ very much for small droplets since, for droplets 0.5 mm to 1.2 mm in diameter, the radiation contribution ranges from 10 to 35% of the total heat flux. However,  $B'$  is the proper parameter to use.

Yuen and Chen (1978) suggest the following correlation:

$$Nu_A (1 + B) = 2.0 + 0.6 Re^{1/2} Pr^{1/3} \quad (21)$$

where properties are evaluated at mean film conditions except for the density in the Reynolds number which is the gas free stream density.

Hoffman and Ross (1972) made a theoretical study of the effect of mass transfer on heat transfer to an evaporating droplet. They concluded that mass transfer causes a decrease in the convective heat transfer rate, the relative decrease being independent of the Reynolds number and function of the modified Spalding number  $B'$  only. They suggested:

$$Nu_A / Nu_T = (1 + B')^{-0.6} \quad (22)$$

for  $0 < Re < 500$ : A plot of  $(Nu_A / Nu_T)$  versus  $(1 + B')$  in this range of Reynolds number showed that this correlation does not differ too much from Hoffman's (1962).

Many theoretical analyses on the evaporation of droplets in a high-temperature medium have also been performed very recently at the High-Temperature Institute in the USSR. Mostinskiy and Laurden (1976, 1977) have published two papers on the modelling of the solvent evaporation of a droplet and a cloud of droplets in a hot gas stream. They considered several models,

the exact model being determined by the form of the dry residue: monolithic particles, porous particles and hollow particles. They used the following correlation for heat transfer:

$$Nu = (2.0 + 0.6 Re^{1/2} Pr^{1/3}) \times H_1 \times \phi_r \quad (23)$$

where  $H_1$  is the correction for high mass transfer rates.

$$H_1 = (1/B) \ln (1 + B) \quad (24)$$

and  $\phi_r$  is a correction to account for a moderate radiation flux to the drop:

$$\phi_r = 1 + F/0.5 (2 + 0.6 Re^{1/2} Pr^{1/3}) H_1 \quad (25)$$

where  $F$  is the ratio of the radiation flux to the convective flux neglecting the vapour flux and its relative velocity. This second correction was very small indeed and does not appear in the second paper (1977) dealing with the cloud of vapourizing droplets.

Golovin and Pesochin (1974) have carried out an analysis of the evaporation of a single drop of an aqueous solution of potassium carbonate in the combustion products of fuel gases, considering the successive stages of heating the solution, evaporation until saturated solution is formed, subsequent evaporation with a solid phase, and heating of the solid. Several interesting points were made, among which the following should be mentioned:



- ° The influence of radiation on the rate of evaporation became substantial only for drops with a radius over 100  $\mu\text{m}$ . The time required for the complete evaporation of drops of solution with an initial radius of 300  $\mu\text{m}$  increased by about 5% if radiation was neglected at a mean gas temperature of 2 800 K.
- ° The time required to heat the solution to its boiling point was negligibly small, compared to the evaporation time.

Finally, it should be mentioned that when the temperature difference between the gaseous heating medium and a particle or droplet immersed in it becomes very large, it is no longer permissible to use average values for the property of the fluid. This case, called variable property flow, was studied theoretically and experimentally by N.N. Sayegh (1977) in this laboratory. He obtained good agreement between his theoretically-derived correlation and his experimental results. Errors of 25% and over were noted by using the Ranz and Marshall Equation. Independent confirmation of the validity of his work was recently provided by Randhawa (1981), also in this laboratory.

Evaporation in a Superheated Steam Medium  
at High Temperature

Superheated steam drying is not a new idea (Hausbrand, 1912) but the number of commercial applications has been somewhat prevented by the lack of suitable equipment and by the corrosive conditions encountered.

When a liquid evaporates into a medium of its own vapour, the resistance to mass transfer becomes negligibly small and the droplet temperature approximates the boiling point temperature corresponding to the operating pressure within the spray drying chamber, and to the concentration of the solution which is being dried.

Numerous experimental studies have been carried out to compare the rate of evaporation of droplets of solution in air and in superheated steam, leading to different conclusions. Chu et al. (1959) compared the rates of evaporation of pure water in air and in superheated steam at atmospheric pressure and they concluded that for the same mass flow rate and temperature difference between the drying gas and the liquid surface, the evaporation rate in steam was faster. In another work on water, 1-butanol and benzene, Chu et al. (1953) showed that evaporation in superheated vapours is faster than in air, except when the superheated vapour is close to the saturation temperature of the

liquid. According to them, a critical evaporation temperature could be determined at which the rate of evaporation into superheated vapours and into air would be equal.

Wenel and White (1951) reached the same conclusion with tests under high pressure. Trommelen and Crosby (1970) investigated the drying behaviour of a pure water droplet, and of a single drop containing dissolved or suspended solids in both air and superheated steam. The temperature of the drying medium ranged from 150 to 250°C and the droplet diameter was 1.56 mm. They found that the evaporation was slower in superheated steam than in air, although it depended strongly on the material being dried. These inconsistent behaviours have led Yashida and Hyodo (1970) to define an inversion point, at which the evaporation rate is independent of the dewpoint of the air. Below that temperature, the evaporation rate decreases with increasing air humidity and above it evaporation is faster in superheated steam.

A theoretical investigation of the drying behaviour of a single droplet of a solution in its superheated vapour has been made by Moyers (1980) based on the experimental data obtained by Trommelen and Crosby (1970). He used the Ranz and Marshall correlation, corrected for high mass flux.

Toei et al. (1966, 1978) determined the following correlation for the evaporation of water drops in superheated steam

and in mixtures of air and steam, in the range  $9 < Re < 120$  and  $0.70 < Pr < 1.0$ :

$$[Nu \text{ or } Sh (p_{BM}/p)^{-0.20}] = 2.0 + 0.65 Re^{1/2} [Pr \text{ or } SG]^{1/3} \quad (26)$$

Lee and Ryley (1968) reported the empirical factor in the Ranz and Marshall Equation to be 0.74 instead of 0.60 from their study on the evaporation of non-spherical water drops into superheated steam. It is to be noted that they found the coefficient to be 0.60 for evaporation in air under comparable conditions.

Finally, according to most of the authors previously cited, the Ranz and Marshall correlation is well applicable to the evaporation of a spray of liquid droplets in gases and in superheated vapours at low temperature for  $1 < Re < 450$  and  $Pr < 250$ . At high temperature, we have also seen that a correction based on the modified Spalding number is required, and for very high temperature differences, the correlation for heat transfer developed by Sayegh (1977) must be used.

## PLASMAS

### DEFINITION OF THERMAL PLASMAS

A plasma may be defined as an electrically quasi-neutral partially or completely ionized gas, the properties of which depend on the pressure, the temperature, the composition and the nature of the gas. This term is so general and includes such a wide range of gas states that a distinction is commonly made between the "high-temperature plasmas" which are involved in nuclear reactions and power generation and correspond to temperatures as high as  $10^6$  K, and the "thermal plasmas" which are used in chemical processing and chemical reactions at temperatures between 3 000 K and 20 000 K and at pressures near atmospheric and higher. In addition, low temperature plasmas or "cold plasmas" are formed under vacuum conditions, and exhibit high electron temperatures, which are not in equilibrium with the low temperature of the other species. Only thermal plasmas will be considered in the following review.

A thermal plasma is thus a quasi-neutral, partially ionized gas which is composed of molecules, atoms, electrons, and positive and negative ions. It should contain at least in the order of 1 percent of its molecules in the ionized state (Thring, 1966). Since the dissociation of molecules and the excitation and ionization of atoms are all endothermic processes, the higher the temperature the higher the degree of dissociation and ionization of the plasma gas.

#### GENERATION OF THERMAL PLASMAS AND THEIR COMMON APPLICATIONS

##### Generation Processes

Most thermal plasmas used in industrial applications are generated by means of high-intensity electric discharges although they can also be produced in high-temperature combustion systems, or in detonation and shock waves. Two basic types of electric arc devices have been developed until now, as described below.

##### a. Thermal Plasmas Generated by Radio-Frequency Torches

Radio-frequency induction heaters produce relatively low velocity plasmas at temperatures up to 20 000 K in the fire ball. The plasma gas is introduced tangentially at the end of a quartz tube which is surrounded at the other end by a water-cooled induction coil. The ionization process must be initiated by some

external discharge. The main advantage of this plasma torch lies in the fact that there are no electrodes and thus no contamination of the plasma. In addition, it is possible to work with either inert or reactive gases (such as oxygen) without any corrosion problem and the plasma jet has a lower velocity than in other devices. Nevertheless, both the amount of input power and the efficiency of these torches are relatively low; thus they are mainly used in laboratory experiments or involved in the small-scale production of ultrapure materials such as very high melting point metals or single crystals of sapphire, ruby, zirconia and niobium. Instead of inductive coupling, as described above, capacitance coupling can also be used, with similar results. This type of plasma generator has, however, received little attention.

b. Thermal Plasmas Generated by DC Electric Arcs

On the contrary, direct or alternative current electric arc devices can produce thermal plasmas at a high energy level and are used in many industrial applications. They may be divided into the transferred arc types and the jet or non-transferred arcs.

In a transferred arc torch, the arc is externally struck between a cathode and a piece of metal which acts as an anode, for the purpose of cutting or welding the latter. A very important

application of these arcs appears to be in the field of metallurgical operations, where the anode now consists of a molten metal. The reader is referred to the thesis of Mehmetoglu (1980) and Choi (1981) for details on the operating characteristics of these arcs.

In a non-transferred arc torch, the anode is a water-cooled copper nozzle out of which the plasma jet emerges at a high velocity and temperature. The cold gas may be introduced axially into the annular gap between the two electrodes or tangentially to produce a swirl. The arc is thus forced to strike within the constricted part of the nozzle, which results in an increase of the temperature and the velocity of the arc column (thermal pinch effect). The cathode which emits electrons is usually made of thoriated tungsten while the anode nozzle is made of copper. The common operating gases are argon, helium, hydrogen and nitrogen. The voltage drop between the electrodes varies from gas to gas, the highest value being for hydrogen (about 120 volts) and the lowest one for argon (20 volts).

The commercial DC plasma torches produce gas streams at temperatures between 8 000 K and 20 000 K and at velocities up to 1 000 m/s, with power inputs ranging up to 10 MW. Their thermal efficiencies are in the range 60-90%, depending on the design and power rating.



The most successful current applications of these plasma torches can be found in industrial ceramic and metallurgical processes as a source of high temperature heat:

- ° cutting and welding of metals and refractories
- ° surface treatment of materials such as spray-coating of high temperature resistant ceramics
- ° particle spheroidization
- ° reduction of mineral ores

An extensive review of these processes was done by Sayce (1971) and by Aubreton and Fauchais (1978).

Considerable work on transferred arcs has been carried out in these laboratories. A good review on their properties and applications can be found in the article by Gauvin et al. (1981).

#### SOME FUTURE APPLICATIONS

The "plasma reactor" may be defined as a chemical reactor where the synthesis of high-value products and especially endothermic compounds could be achieved by high-temperature reactions. It would require a quenching arrangement to prevent the high-temperature thermodynamically stable compounds from decomposing at lower temperature. Besides, a thermal plasma can also be considered as a source of reactive ionized species or as

a source of high-intensity radiations and it could become a way of initiating very fast reactions involving free-radical mechanisms, these reactions being then frozen out by quenching to produce metastable compounds of industrial importance.

The range of products that may be synthesized in a plasma reactor is very wide. These are, for instance, nitric oxide, hydrogen cyanide, cyanogen, acetylene, silica or titanium dioxide pigments and pure boron. As a matter of fact, the last four are already being produced commercially at present.

#### ADVANTAGES OF PLASMA TECHNOLOGY

Plasma technology is becoming more and more important in the high-temperature processing field. Process heat is increasingly provided by electricity instead of hydrocarbon fuel sources, due partly to the ever increasing price of the latter, but also due to the realization that, for a high-temperature working process, the availability (or energy) of electrical energy is considerably higher than that of a combustion process. Thus, comparison of energy costs based on calorific value is meaningless. This statement is less valid for processes operating at 1 000°C or less.

Plasma technology offers a number of favourable features which may make it more attractive than conventional techniques (electric furnaces, induction furnaces, fluidized beds) in

situations where they can be fully utilized.

- ° First of all, plasmas allow continuous operation of the process under controlled conditions. Many conventional metallurgical processes are batch.
- ° Plasmas allow rapid heat-up and quench of the whole system. They are thus convenient, flexible and efficient tools to reach temperatures as high as several thousands of degrees, in either inert or reactive atmospheres.
- ° Plasma processes are particularly well suited to handle small particles, whereas these particles must be agglomerated or pelletized for many conventional applications.
- ° In the case of particulate systems, an extremely hot environment provides the particles with very high reaction rates or heat transfer rates. These high rates permit shorter residence times in order to accomplish the desired transformation and, in turn, a short residence time allows reasonable throughput in a very small volume. This must be compared with the huge equipment needed for

particulate systems when the particles have to stay several hours in the reaction zone to give the desired products. Good examples of such enormous equipment are the conventional spray-dryers used in industry.

- ° Short residence time and controlled atmosphere can contribute to large improvements in product quality whereas such improvements would introduce severe requirements in particulate conventional processes.
- ° Finally, it appears that important savings in capital expenditure, space requirements and pollution control are likely to be achieved by using this technology.

A very recent article by W. Lugscheider (1981) compares the performance of large plasma furnaces (15 and 45 metric tons) with electric furnaces of equivalent capacity, for the production of steel. The advantages of the plasma furnace are indeed impressive and indicate a good possibility that it might displace at least some of the traditional electric furnace applications.

## SUPERHEATED STEAM PLASMAS

### Generation and Applications

Although plasmas of water vapour have been experimentally produced in research laboratories in the past, only one industrial application has been proposed in the literature, but, to the author's knowledge, has not been practiced. It may be that researchers in the past have not realized the potential of steam plasmas. But there is no doubt that the development of these plasmas has been restrained by the lack of commercial equipment available to generate such plasmas. The only torch currently available has a nominal power rating of 200 kW. It was developed by Alusuisse in Switzerland, based on the Kugler patents (1972 to 1975), for the industrial application referred to above, namely the production of very fine silica. It is basically a transferred DC torch, the arc being struck between a consumed carbon rod cathode and a rotating copper anode some 15 cm away. Water is introduced tangentially at high velocity in the cylindrical torch and the plasma steam is generated under pressure from its evaporation. The great disadvantage of this torch is that the plasma is heavily contaminated with CO and carbon particles, which may explain why its commercialization was never pursued. The only other literature reference of note concerns the Columbia University team of Sheer, Korman and Dougherty (1979) who

studied the gazification of coal in a steam plasma generated by a 120-kW transferred arc.

#### ADVANTAGES AND DRAWBACKS OF STEAM PLASMAS

In addition to the advantages inherent in the use of plasma technology, as previously mentioned, the use of steam plasmas offers the following benefits:

- Steam is a low cost gas and it is relatively inexpensive to compress.
- Steam has an extremely high enthalpy compared to other common plasma gases.
- It has a very high chemical reactivity when dissociated.
- Gas-solids separation and latent heat recovery are easily achieved by condensation.
- It is suitable for high-pressure applications.

One major drawback when planning the utilization of such a steam plasma is the very little amount of information available on steam at such high temperatures. It is known that steam begins to dissociate at about 2 000 K, whereas it is completely dissociated at 6 000 K under atmospheric pressure. Ionization begins at 8 000 K (Gauvin, 1981).

Calculated values of the equilibrium thermodynamic

properties under pressures of 1 and 100 atmospheres, together with the molecular weight of steam, have been tabulated by Ihara (1977) for temperatures from 1 000 K to 6 000 K, the reference state being superheated steam at 1 000 K. Transport properties such as thermal conductivity or viscosity are particularly needed. They can be estimated by use of the Leonard-Jones potential parameters (Reid and Sherwood, 1966), but this will give only an order of magnitude of the actual values, as we do not know much about the dissociated state of steam at these high temperatures. Approximated values of viscosity can be extrapolated from the data of Potter (1949) (assuming that  $Pr = 1$ ):

$$\mu = 3.366 \cdot 10^{-8} T + 9.57 \cdot 10^{-7} \quad (27)$$

and values of thermal conductivity are obtained from Hilsenrath et al. (1955):

$$k = k_0 + 4.593 \times 10^{-3} (10^{0.88} \times 10^9 / T^4 - 1) \quad (28)$$

$$\text{where } k_0 = 6.4753 \times 10^{-3} \sqrt{T(1 + 1737.3 \times 10^{(-12/T)})/T} \quad (29)$$

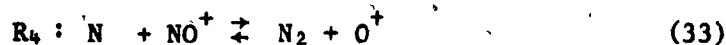
Some more data can be found in several papers from Burnhorn and Wienecke (1970) and Gross et al. (1968).

The density of steam can be calculated by means of the ideal gas law, for relatively low pressures.

It is interesting to note that a theoretical study has recently been carried out by Poissant (1978) to calculate the composition of a complex mixture under partial equilibrium at high temperature. In particular, it was applied to a mixture of nitrogen and water in the plasma state. At a temperature of about 5 000 to 6 000 K, the partial equilibrium is determined by two reactions:



And at a temperature of 4 000 K, two more reactions have to be considered:



and the presence of nitric oxide can therefore be suspected.

Another drawback is that a reliable generator capable of producing relatively pure water vapour plasmas simply does not exist. Dissociated steam being very aggressive to many of the metals used in the assembly of commercial torches (W, Cu, brass, steel), the problem is largely one of materials of construction. There is little doubt that a reasonable research effort will



overcome this deficiency.

The McGill plasma team has decided to pioneer the development of steam plasmas with industrial applications in view. In a recent paper (Gauvin and Avedesian, 1981), the following applications were reviewed: spray drying, gazification of biomass, hydrocracking of heavy oils and submerged combustion. In addition to the spray drying application - which is the object of this thesis - a study of the gazification of peat has just been completed, while the study of the hydrocracking of heavy oils has recently begun.

# NOMENCLATURE

- a - constant in Equation (11)
- a', b' - constants in Equation (15)
- C<sub>p</sub> - heat capacity of drying gas (J/kg K)
- d<sub>o</sub> - nozzle orifice diameter (m)
- D<sub>v</sub> - drying gas diffusivity (m<sup>2</sup>/s)
- D - droplet diameter (μm or m)
- D<sub>m</sub> - median diameter (μm)
- D<sub>MAX</sub> - stable largest droplet diameter, according to Mugele and Evans (μm)
- D<sub>VS</sub> (or D<sub>3,2</sub>) - Sauter mean diameter (μm)
- D<sub>10</sub> - droplet diameter corresponding to V(D) = 10%
- D<sub>90</sub> - " " " " V(D) = 90%
- D<sub>84.13</sub> - " " " " V(D) = 84.13%
- f(D) - relative frequency distribution function of droplet diameters

|            |                                                                                                |
|------------|------------------------------------------------------------------------------------------------|
| $FN$       | - flow number $[(m^3/s)/(N/m^2)^{1/2}]$                                                        |
| $h$        | - actual heat transfer coefficient from drying gas to droplet $(J/m^2 \text{ sK})$             |
| $k_f$      | - average thermal conductivity of drying gas in the boundary layer around the droplet $(W/mK)$ |
| $k_G$      | - mass transfer coefficient                                                                    |
| $\dot{m}$  | - evaporation rate $(kg/s)$                                                                    |
| $P$        | - nozzle atomizing pressure $(N/m^2)$                                                          |
| $P_{BM}$   | - average partial pressure of air in the boundary layer adjacent to the droplet $(N/m^2)$      |
| $p$        | - total pressure of chamber $(N/m^2)$ in Equation (26)                                         |
| $Q$        | - nozzle capacity $(m^3/s)$                                                                    |
| $q_r$      | - rate of heat transfer by radiation $(J/s)$                                                   |
| $R$        | - universal gas constant $(J/mole \text{ K})$                                                  |
| $S$        | - size distribution parameter                                                                  |
| $T$        | - absolute temperature $(K)$                                                                   |
| $\Delta T$ | - heat transfer driving force                                                                  |
| $u(D)$     | - function in Equation (13)                                                                    |
| $V(D)$     | - cumulative volume fraction of droplets having a diameter $< D$                               |

- $v_f$  - relative or terminal droplet velocity (m/s)  
 $v_v$  - vertical velocity component of the liquid jet  
 at the exit of the nozzle (m/s)  
 $v_t$  - tangential velocity component of the liquid jet  
 at the exit of the nozzle (m/s)  
 $y(D)$  - function in Equation (10)

### G r e e k   S y m b o l s

- $\delta$  - size distribution parameter  
 $\theta_m$  - maximum cone angle of the spray (rad)  
 $\lambda$  - latent heat of vapourization (J/kg)  
 $\alpha$  - surface tension of the drying gas/feed system (N/m)  
 $\mu$  - viscosity (kg/ms)  
 $\rho$  - density (kg/m<sup>3</sup>)

### D i m e n s i o n l e s s   G r o u p s

- $B$  - Spalding Number,  $C_p \Delta T / \lambda$   
 $B'$  - modified Spalding Number,  $C_p \Delta T / (\lambda - q_r / \dot{m})$   
 $E$  - entrainment parameter  
 $Nu_A$  - actual Nusselt Number,  $h D / k_f$   
 $Nu_T$  - true Nusselt Number, in the absence of evaporation

- $P_r$  - Prandlt Number,  $C_p \mu_f / k_f$   
 $Re$  - Droplet Reynolds Number,  $D V_f^l / \nu_f$   
 $Sc$  - Schmidt Number,  $\mu_f / D_v \rho_f$   
 $Sh$  - Sherwood Number,  $k_g D_{RT} / D_v$

### S u b s c r i p t s

- $l$  - liquid  
 $a$  - drying gas  
 $f$  - drying gas at the mean temperature of the  
 boundary layer around the droplet

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EXPERIMENTAL SECTION

PART I

DESIGN AND PERFORMANCE OF A PILOT-PLANT SPRAY DRYER  
USING PLASMA-GENERATED SUPERHEATED STEAM

## EXPERIMENTAL SECTION

### PART I

#### INTRODUCTION

Spray drying may be defined as a continuous operation in which the solution, slurry or suspension to be dried is introduced in a drying chamber in the form of a finely-divided spray of droplets, in the presence of a heated gaseous stream which acts as the drying medium, and provides the latent heat of vaporization required to evaporate the liquid phase. Today, spray drying is a mature technology which finds many applications in the chemical field and in food and pharmaceutical processing industries for the production of dried products with highly specific properties. In addition to these traditional uses, it is capable of handling very complex production problems such, for example, as the production of complex mixtures of solids, when each component must be evenly proportioned in each particle of the powder or in the preparation of molecular-sieve cracking catalysts from a gel-catalyst mix.

The ever-increasing number of applications illustrates the ability of the spray drying technology to meet a wide range of specific and exacting products requirements. On the other hand, the large diversity of spray dryers designs and arrangements underlines the fact that spray drying design is still an art, highly dependant on pilot-plant test data and industrial experience (Masters, 1979); this is partly due to the complexities of the flow patterns generated by the spray entrainment, the recirculation and mixing of the drying gas, and the resulting turbulence within the chamber, and partly to the lack of publications on advanced technology by the manufacturers. Considerable improvements have nevertheless been made in spray dryers design and operation to produce high-quality dried product at lower and lower cost. However, none of these advances has overcome the major weakness of the spray drying process, namely, the large volumetric flow rate of drying gas needed to achieve the required degree of moisture removal, the correspondingly low overall thermal efficiency of the system (seldom exceeding 60%) and the resulting intensive capital and operating costs for the whole process, including gas heating, gas-product separation and effluent scrubbing and/or treatment.

For many years, this laboratory has been engaged in the study of the fundamentals of particulate systems and, more recently, in the field of plasmas and high-temperature technology. Thus, based on the large experience gained in these fields, a novel approach to spray drying was proposed very recently by Gauvin (1981),

namely, the use of a plasma of superheated water vapor as a direct and efficient heat source for spray drying. The idea of using superheated steam as the drying medium in a drying operation is certainly not new (Lane and Stem, 1956), but in spite of favourable recommendations from several investigators, there is no evidence of such a use as an industrial process up till now.

Gauvin and Costin (1980) proposed to use superheated steam as the drying medium in a spray drying operation, in which most of the steam would be re-heated and recycled. Specifically, they compared a conventional hot open-air system with a closed one using recycled superheated steam. Their study was based on a computer simulation developed in 1976 by Gauvin and Katta to predict the size of a drying chamber required for a given evaporative load and for given product specifications. The economics of both systems were also analyzed which indicated that, under strictly comparable conditions, for a typical medium-size installation of 2 300 kg/h water evaporative capacity, savings of 33% in capital costs and 21% in operating costs could be achieved by using superheated steam.

The second and more radical approach considered by Gauvin (1981) was inspired from the Atomization Suspension Technique developed in the fifties by Gauvin and his colleagues (Gauvin and Gravel, 1962), and from the increasingly promising features of the high-temperature plasma technology. The analysis was still based



on the computer simulation of Gauvin and Katta (1976), simplified however for a two-dimensional situation since no vortex flow was involved. It showed clearly many advantages for the system, in spite of the great number of assumptions which were made particularly for the entrainment and mixing phenomena without any experimental support. It indicated, for example, that a steam plasma combines the ability of a plasma jet to act as an efficient direct source of heat while maintaining the evaporation and drying processes in a pure superheated steam atmosphere, most of which is provided by the evaporation process, allowing thus a large reduction in chamber size (since no large volume of drying medium was needed). It also offered the possibility of operating under pressure (which is impossible in conventional spray dryers owing to their large sizes), and the absence of blowers and compressors in the system. A high thermal efficiency was claimed for the overall operation if the effluent superheated steam is effectively condensed to produce clean steam for plant use. The major shortcoming faced by that system was that it is applicable only to the drying of non-heat-sensitive materials, because of the high temperature of the drying steam in the upper regions of the drying chamber.

These promising conclusions have led to the development of an experimental program to investigate the proposed system and the present study is aimed at assessing its technical feasibility.

Basically, the operation as proposed by Gauvin (1981), consists of atomizing the solution to be dried in the form of a spray of fine droplets into the top of a heavily insulated stainless steel cylindrical chamber; a very small stream of highly superheated plasma-generated steam, typically at 6 000 K, is introduced cocurrently at the top of the chamber; the high enthalpy of that steam ( $73.6 \cdot 10^4$  J/kg or 31 700 Btu/lb at 6 000 K) serves to vaporize the water contained in the droplets. Thus the droplets, during their descent through the chamber, are in contact with a surrounding stream of superheated plasma steam which is progressively diluted and cooled by low-temperature steam generated from the droplets own evaporation. Following separation of the dried product particles entrained by the exit stream from the bottom of the chamber, the effluent superheated steam is condensed to produce clean steam for plant use.

The pilot-plant installation built for the present study was modified somewhat from the above described system, since difficulties were encountered in obtaining a commercial plasma torch operating on steam. None of the conventional plasma devices can operate on steam, owing to the aggressive nature of dissociated water vapour. The one exception, producing a water vapour plasma contained with carbon, was much too large for the purpose of this preliminary study. It was, therefore, decided that it would be quite acceptable to simulate a steam torch with a conventional DC torch operated on nitrogen to which water vapour was added close to the anode-nozzle exit.

The objectives of this study were to test the technical feasibility of the new spray drying approach, to bring out its advantages and identify some of the inherent difficulties which might be encountered in its industrial development. The experimental program, therefore, included first the design, construction and calibration of the pilot-plant installation, followed by the determination of the maximum evaporative capacity and thermal efficiency of the drying chamber under various operating conditions. Several pressure atomizers were used, giving different drop size distributions, and the feed consisted in turn of pure water and aqueous solutions of sodium nitrate ranging from 10% to 30% in concentration. This salt was selected because it does not form any hydrate, has a moderate solubility and no explosive, corrosive or toxic characteristics. Most importantly for the present study, it is quite thermally stable (melting point at  $310^{\circ}\text{C}$ , decomposition at  $380^{\circ}\text{C}$ ).

For a given pressure atomizer, a given feed concentration and for a given power fed to the plasma torch, an optimum range of feed rates was found, above which the salt was incompletely dried and below which it melted. The corresponding thermal efficiencies were calculated afterwards from heat and mass balances on the dryer. Some additional information was obtained from the experimental system such as the steam temperature profile at the centerline of the dryer and the walls temperature profile; finally, useful qualitative observations were made concerning the effect of the high temperatures

involved on the material being dried and on its mode of drying.

All these results are discussed in the first part of the Experimental Section, together with a complete description of the apparatus, of the measurements involved and of the procedure followed. In the second part of the Experimental Section, an attempt is made to test several of Gauvin's assumptions in the light of the experimental data obtained and to modify his 1981 analysis so as to provide a better fit of the experimental results. However, an extrapolation to an industrial size drying installation was not attempted, because of the difficulty of finding dynamic relationships between the entrainment and recirculation phenomena and the chamber geometry and dimensions.



## EXPERIMENTAL

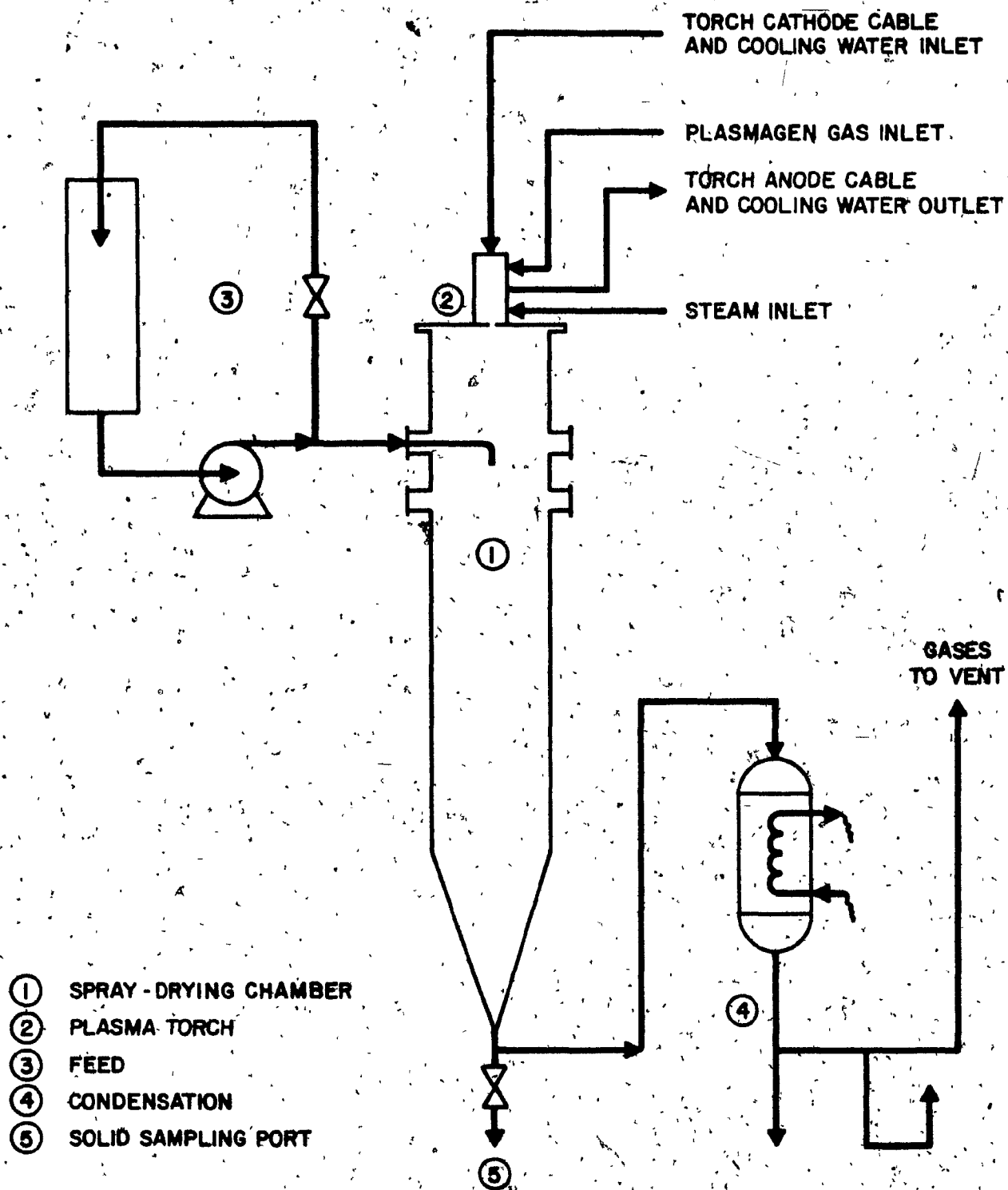
### APPARATUS

The experimental apparatus described in this section has been entirely designed, built, calibrated and tested in the course of this research project. A schematic diagram of the overall system is shown in Figure 1, where five basic interconnected units can be easily distinguished:

1. A cylindrical spray drying chamber.
2. A plasma generation unit.
3. A liquid feed system.
4. A condensation and sampling unit for the effluent gas.
5. A collection device for solid particles.

A detailed description of the five units involved in the experimental apparatus is given below.

FIGURE 1SCHEMATIC DIAGRAM OF APPARATUS



### 1. The Spray Drying Chamber

The spray drying chamber consisted of a vertical cylindrical type 316 stainless steel reactor 30.5 cm (12 in) in diameter and 178 cm (70 in) high, as shown in Figure 2. The upper section was 132 cm high and was provided with two pairs of diametrically-opposed flanged ports; these ports were circular with a 5 cm (2 in) internal diameter; two of these ports, located at distances of 30.5 and 61 cm from the top of the chamber, respectively, were used to insert two thermocouples and the feed line to the atomizer within the reactor and the other two on the opposite side were fitted with pyrex discs to allow visual observation inside the chamber. An additional small port provided another temperature reading 18 cm below the top of the chamber. The bottom section of the chamber was cone-shaped, 46 cm long, and terminated into a 1-inch threaded nipple.

The flanged top of the chamber was surmounted with a removable flat stainless steel plate which was firmly bolted to it, an asbestos joint being used to prevent any leak of steam from the reactor. At the center of the top plate, a 4 cm diameter hole was provided for the plasma torch whose nozzle assembly face rested flush with the inside of the plate; the torch was firmly attached on to the top plate, with an asbestos gasket in between.



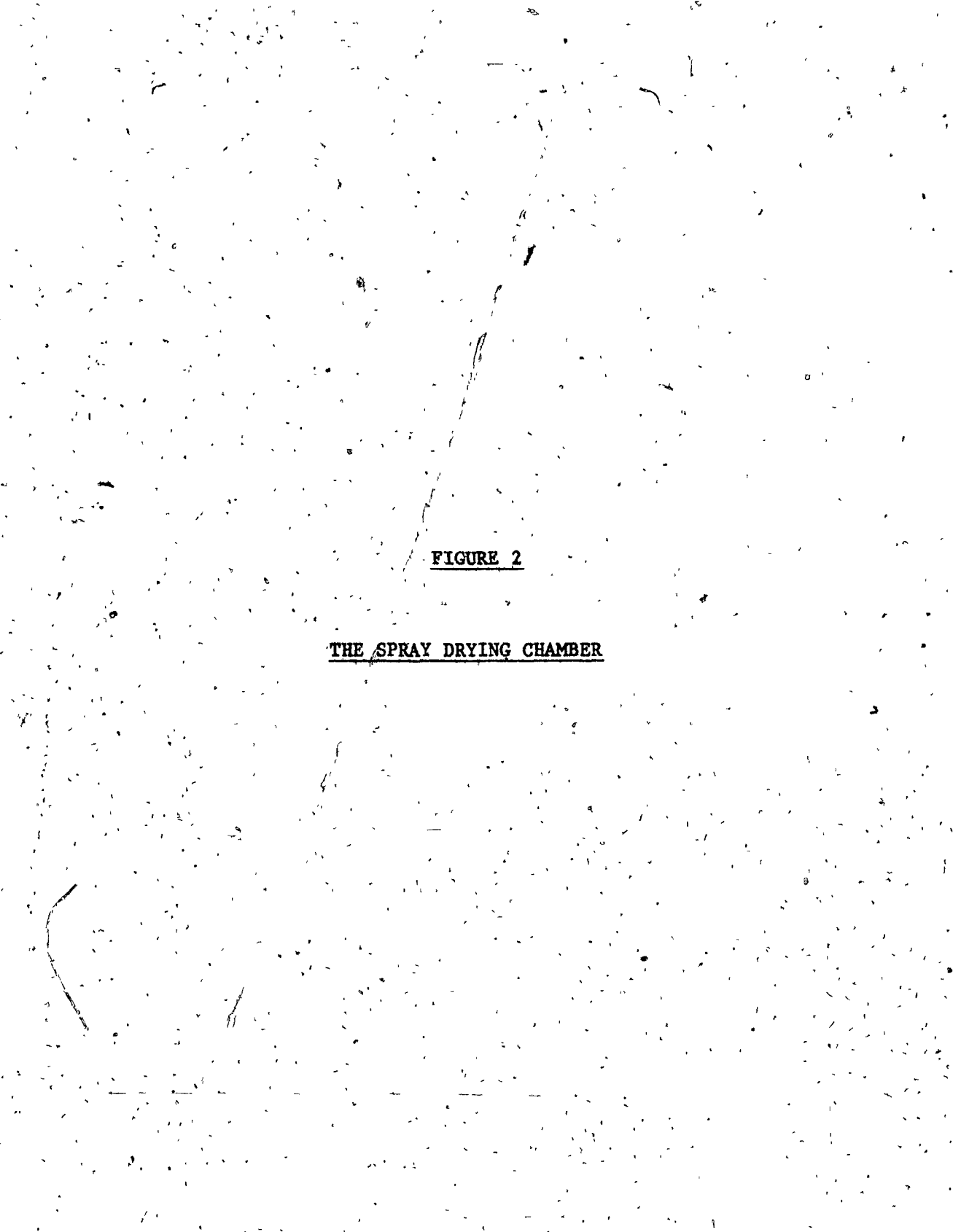
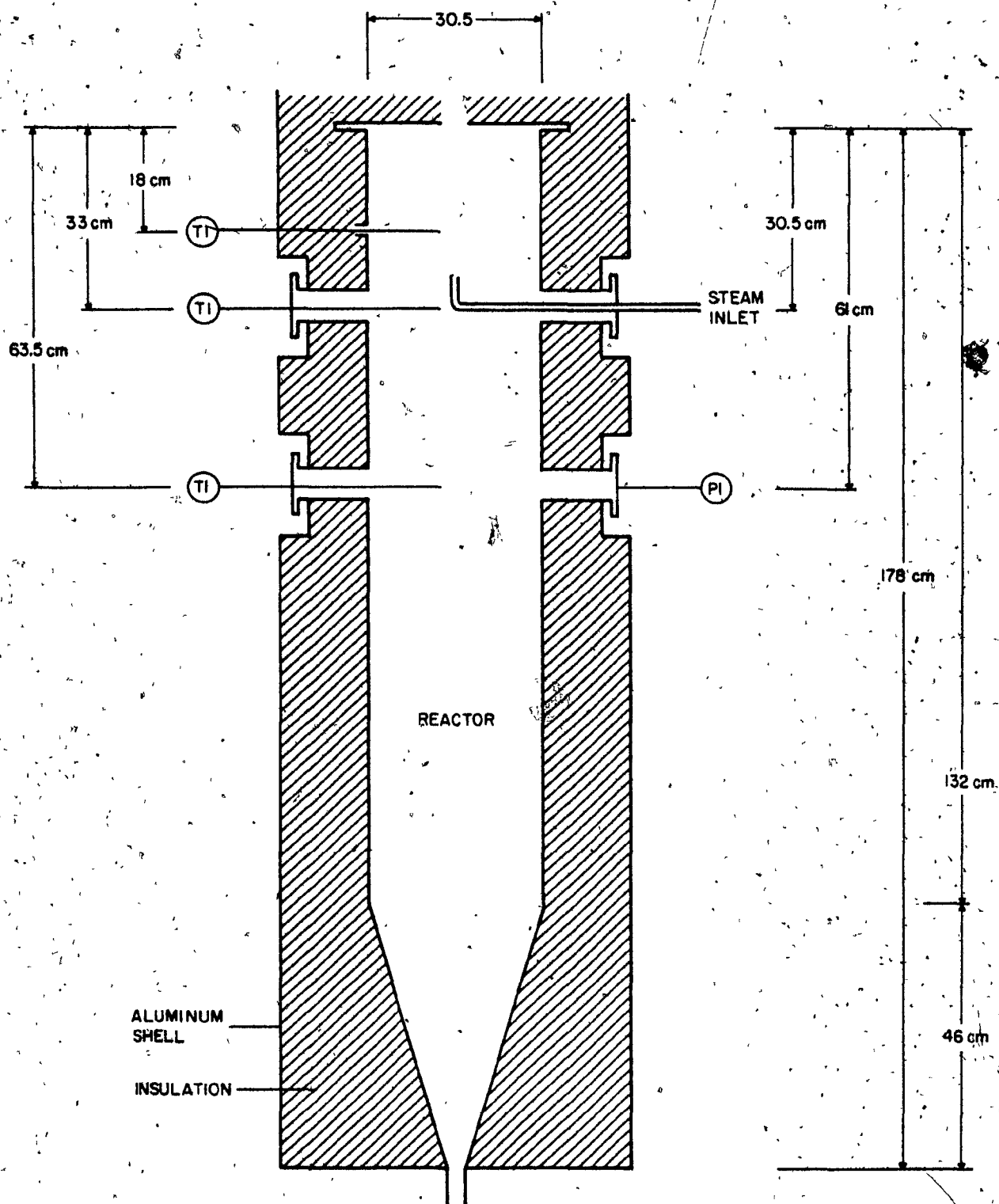


FIGURE 2

THE SPRAY DRYING CHAMBER



The chamber was made of type 316 stainless steel as were all the piping and fittings used in the whole set-up, and the wall and top plate thickness was 3.175 mm (1/8 in).

The chamber was heavily insulated with a 15-cm layer of "Fiberfrax" which is a ceramic blanket made of alumina-silica fibres of high porosity and low thermal conductivity. The insulation was enclosed in a thin (0.8 mm, 1/32 in) aluminum shell.

The whole assembly was supported by four 2-inch pipes; the top of the chamber was located about 285 cm above ground level and the whole apparatus, surrounded by a steel platform and stairs, was easily accessible for cleaning and possible modifications. Figure 3 is a photograph of the apparatus.

The chamber wall temperature was measured at 10.2-cm (4-in) intervals down its length by seventeen surface chromel-alumel (type-K) thermocouples pressed externally against the wall and thermally protected into double-hole ceramic insulation tubes across the "Fiberfrax" layer.

In addition, the chamber centerline temperature was recorded at three different points located 18, 33 and 63.5 cm down from the top plate and also at the exit of the chamber, by means of four shielded type-K thermocouples inserted through the ports. All these thermocouples were connected to a Westronics Strip Chart Multipoint Recorder (model M11E, 24 channels) purchased from Thermo Electric Co., Inc.

FIGURE 3THE INSULATED SPRAY DRYING CHAMBER



#### ii. The Plasma-Generating Unit

The plasma-generating unit as it is schematically shown in Figure 4 consisted of four basic elements: a direct-current power supply, a control console, a plasmagen gas circuit and the plasma torch. The power supply and the plasma torch were purchased from Thermal Dynamics Corporation in Lebanon, New Hampshire, whereas the control console was designed and assembled by Professor Charles Sheer of Columbia University.

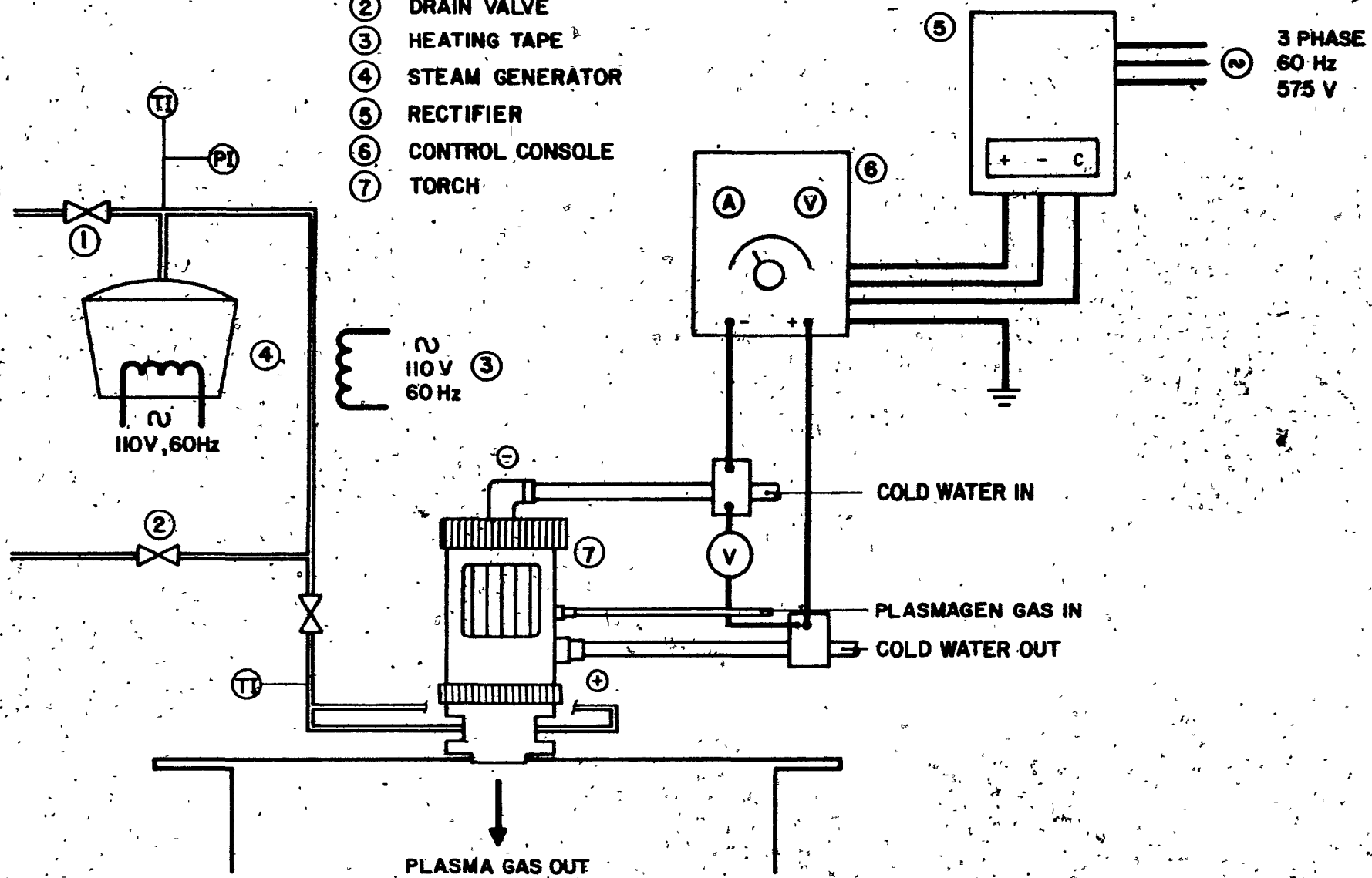
The power was supplied from a Model TDC 1A-40 rectifier whose power rating was 57 kW. The input voltage was three-phase, 60 Hz and 575 volts while the output open circuit provided 160 DC volts.

The control console contained all the necessary control and operating elements for the power supply to the torch. Through a special circuit, it generated the high-frequency, high-voltage spark needed to initiate the electric arc, and it allowed an adjustment and reading of the intensity of the current fed to the plasma torch, as well as its voltage. However, a measure of the voltage directly at the entrance of the torch through an additional voltmeter was preferred as it gave a true measure of the direct power input into the torch. The voltage difference between the two readings was up to 3 volts at 400 A due to the resistance of the leads.

FIGURE 4

DIAGRAM OF PLASMA-GENERATING UNIT

- ① SAFETY RELIEF VALVE
- ② DRAIN VALVE
- ③ HEATING TAPE
- ④ STEAM GENERATOR
- ⑤ RECTIFIER
- ⑥ CONTROL CONSOLE
- ⑦ TORCH





The plasmagen gas control unit was independent from the console and consisted of two parallel circuits, one for argon and the other for nitrogen. Each of these circuits was made up of a gas cylinder, a regulator and a control valve, and both gases came through a single rotameter before entering the plasma torch.

Figure 5 is a photograph of the complete plasma system.

A Thermal Dynamics Model U-51 direct-current torch was used to generate the plasma jet. Figure 6 shows the torch set-up at the top of the spray drying chamber as it was during operation. Five leads may be seen, coming to the torch: the upper pair carried the electrical power and the cooling water going into the cathode while the lower pair carried the electrical power to the anode and also the cooling water leaving the anode. The central smaller lead was for the plasmagen gas inlet.

The working section of that torch was really the copper nozzle assembly located at its lower end, which contains the electrodes. Figure 7 is a schematic cut-away of this nozzle assembly which shows its essential parts, as follows:

- ° A cathode which was a tungsten rod alloyed with thorium (2%) to improve its electron-emitting properties, about 12 mm (0.5 in) in diameter, with a 45-degree half-angle conical tip. Water-cooling of the rod prevented excessive heating and vaporization of the tip.

FIGURE 5.

PHOTOGRAPH OF THE PLASMA-GENERATING UNIT SET-UP

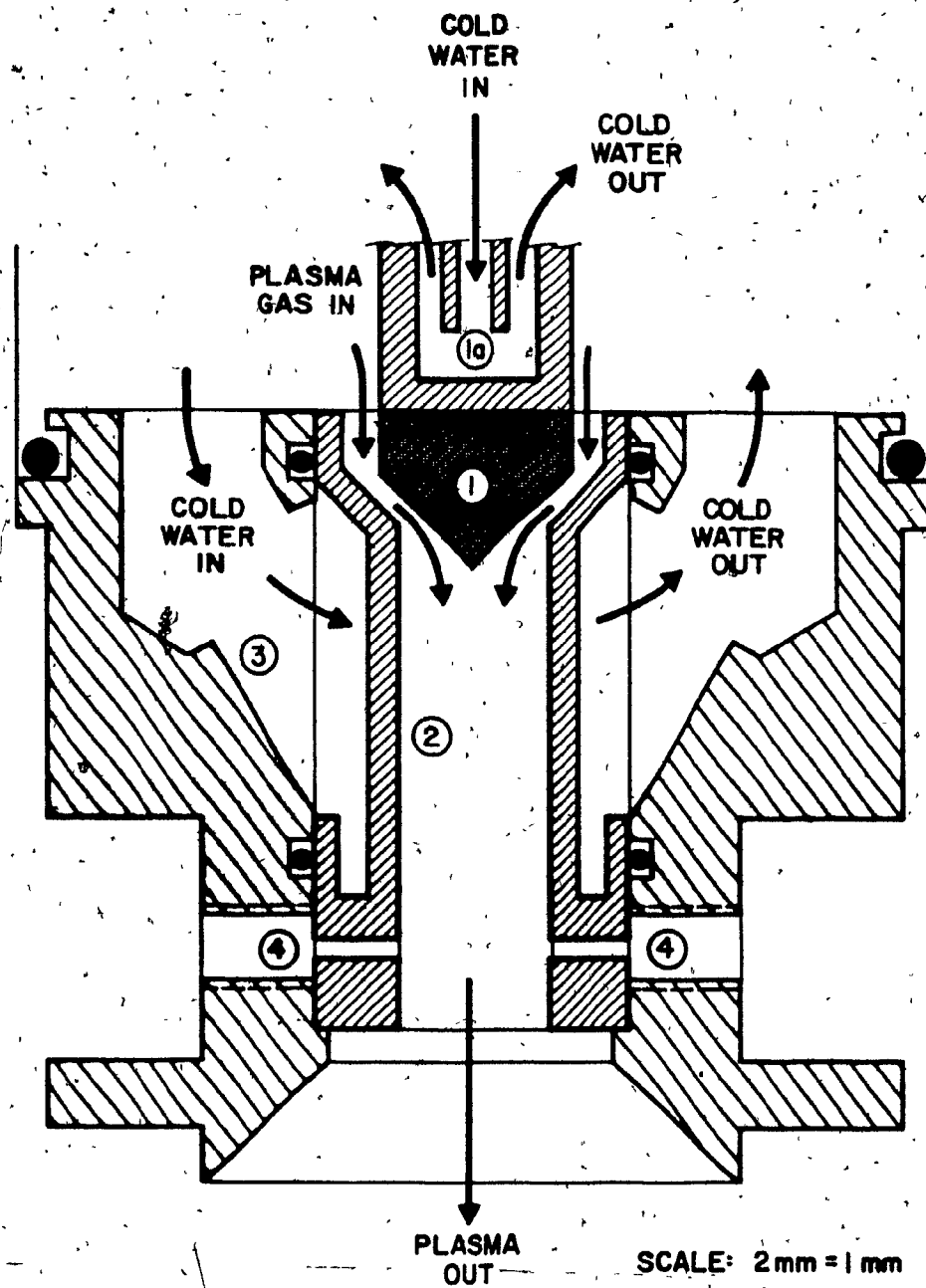


FIGURE 6PHOTOGRAPH OF PLASMA TORCH ON TOP OF REACTOR



FIGURE 7

SCHEMATIC CUT-AWAY OF NOZZLE ASSEMBLY



- 1. THORIATED TUNGSTEN
- 1a. CATHODE HOLDER
- 2. COPPER NOZZLE - ANODE
- 3. NOZZLE BODY
- 4. STEAM INJECTION PORTS

- ° A copper anode-nozzle concentric with the cathode, having an inside diameter of about 9 mm, an outside diameter of about 21 mm and a total length of 40 mm. This anode was also heavily water-cooled during operation.
- ° A nozzle body which maintained the cathode and anode at a fixed distance from each other and excluded the ambient air from upstream of the nozzle.

Two diametrically-opposed small holes were drilled through the nozzle body close to the exit of the anode and at a point just downstream of where the arc was striking. These ports which are generally made to inject powders into the core of the plasma jet were used in the present experiments as steam injection ports; for that purpose they were somewhat enlarged and insulated with ceramic tubes.

Both cathode and anode were replaceable since they tended to wear out rapidly during operation due to gouging by the arc because of mis-alignment of the electrodes, presence of saturated steam and most of the time, because of the occurrence of water leaks from the cooling circuits.

In order to start the plasma jet, a high-frequency high-voltage discharge was struck between the cathode and anode, while argon gas was fed to the torch. The high voltage between the



electrodes caused local ionisation of the gas and free electrons, accelerated toward the anode by the electrical field, caused further ionisation of the gas by collision processes. It resulted in increasing the kinetic energy of the gas and consequently its temperature within the jet. As soon as the arc was ignited, the gas flow was adequate to blow the arc further in the nozzle and to distribute it over a wider area.

#### iii. The Steam Injection Circuit

That injection circuit is shown, together with the plasma generating unit, on Figure 4. Saturated steam was generated in a Presto pressure-cooker. It was slightly superheated (106°C) by means of a heating tape before being introduced into the torch, to prevent its condensation within the small ports.

#### iv. The Liquid Feed System

The most important element in the liquid feed circuit was indeed the atomizing nozzle which generated the spray. The selection and operation of an atomizer is of major importance in spray drying since the initial drop size distribution that it generates largely determines not only the size and quality of the dried product, but also the evaporation and drying rates and, consequently, the capacity and efficiency of the dryer.

Among the various types of atomizers, a pressure nozzle was really the most suitable for the present work because no

dilution by a secondary gas (such as in pneumatic nozzles) could be tolerated and also because of the small diameter of the drying chamber. The major drawback of this type of atomizer was indeed that both the feed flow rate and the drop size distribution were controlled by the atomizing feed pressure but this did not appear as too severe a restriction, considering the original purpose of the work; besides, these atomizers presented some other advantages such as their simplicity of operation, the very small symmetrical spray cones that they could generate and the good uniformity of their drop size distribution.

Three pressure nozzles were selected since they corresponded to the expected operating conditions within the dryer and presented a fairly uniform and symmetrical spray pattern when visually observed; they were all non-swirl pressure nozzles, supplied with internal strainers. The first two were Monarch full-cone pattern nozzles, type HV, with a  $30^\circ$  spray angle, and a 4.0 and 4.5-U.S. Gallons/h capacity; they were made of 316-stainless steel and principally used in domestic oil burners; they were used respectively with pure water and nitrate sodium solutions since the change in orifice size allowed the atomizing pressure and consequently the drop size distribution to be within the same range although the feed mass flow rates were much different. The third one was a Spraying Systems hollow-cone spray pattern nozzle, type LNN2, with a  $75^\circ$  cone angle and a 3.2-U.S. Gallons/h capacity; it was made of 303-stainless steel and designed for spraying water. The respective characteristics of all three

nozzles are listed in Table I. The third atomizer was operated with pure water only, for the purpose of investigating whether the large spray angle it generated and the resulting more or less pronounced impingement of droplets on the dryer wall had an important influence on the dryer evaporative capacity and efficiency.

The feed solution was kept in a 22-liter cylindrical glass tank which was graduated from top to bottom (Figure 8). The liquid feed was directed to the atomizing nozzle through a relatively simple circuit made of 3/8-in copper tubing and pressure fittings; that circuit consisted of a Neptune high-pressure low-capacity displacement pump, a Brooks high-pressure rotameter and needle valve, and several pressure and temperature indicators. The pulsations induced in the system by the displacement pump were reduced to a negligible value by means of a 0.5-liter high-pressure stainless steel expansion tank located at the exit of the pump.

The feed flow rate was set by a rough positioning of the pump piston stroke along a calibrated scale and then precisely adjusted at its desired value by means of the high-pressure needle valve located below the rotameter. The feed flow rate was metered by reading the metric scale on the feed tank, as the rotameter readings were very sensitive to pressure variations within the line and changes of operating conditions due to nozzle partial clogging or erosion. During the first tests on water, these feed flow rate values were checked by measuring the amount of condensate at the

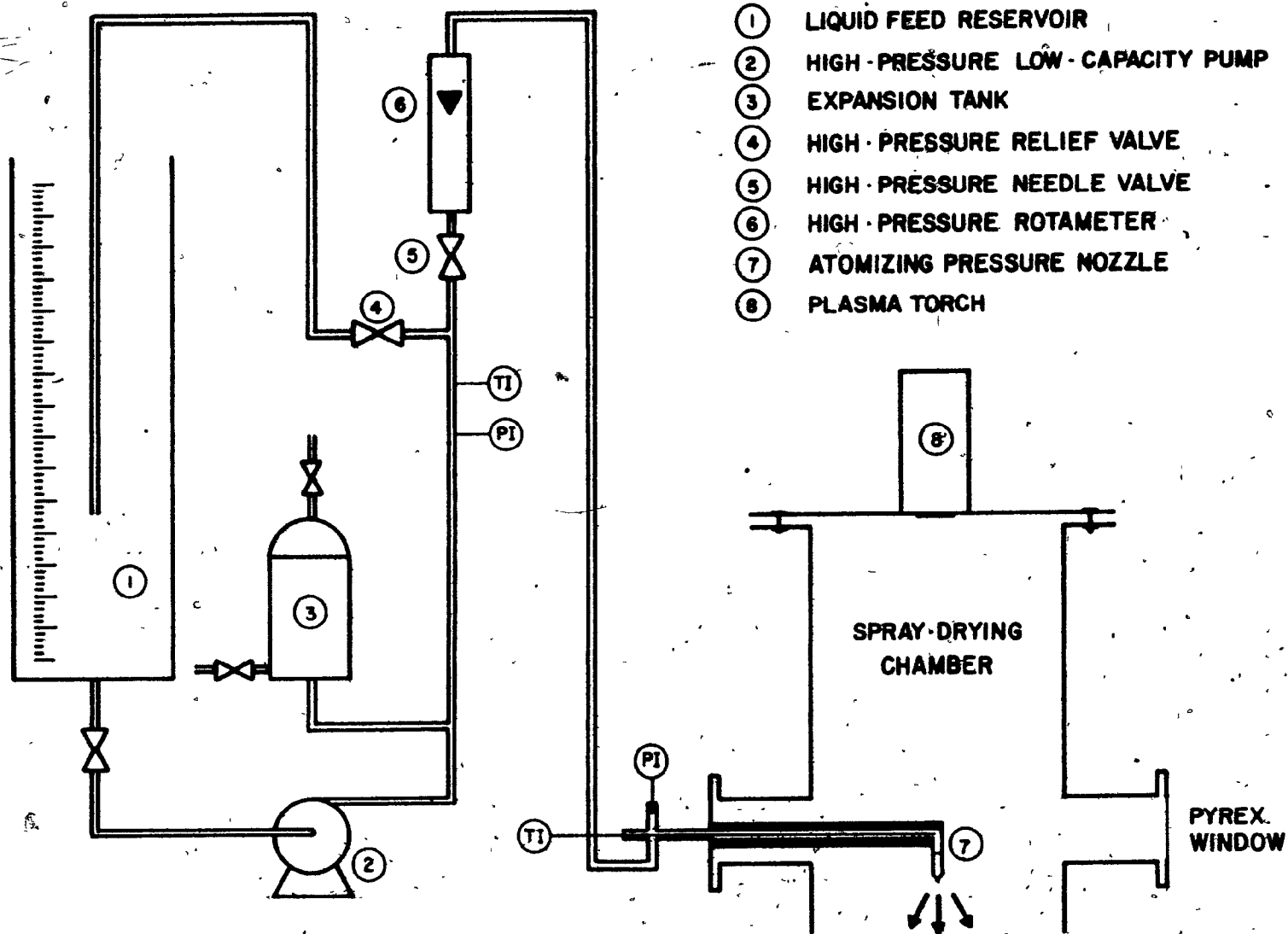
TABLE I

ATOMIZING NOZZLES CHARACTERISTICS

| NOZZLE TYPE                  | SPRAY PATTERN | SPRAY ANGLE<br>at 100 Psi<br>(590 kPa) | STANDARD CAPACITY<br>at 100 Psi<br>(590 kPa) | ORIFICE DIAMETER<br>mm |
|------------------------------|---------------|----------------------------------------|----------------------------------------------|------------------------|
| MONARCH HV<br>4.0            | full-cone     | 30°                                    | 15.1 L/h                                     | 0.533                  |
| MONARCH HV<br>4.5            | full-cone     | 30°                                    | 17 L/h                                       | 0.559                  |
| SPRAYING SYSTEMS<br>1/4 LNN2 | hollow-cone   | 75°                                    | 12.1 L/h                                     | 0.711                  |

FIGURE 8

LIQUID FEED UNIT



exit of the system and the agreement between the two sets of values was fairly good.

A horizontal 1/4-in stainless steel pipe connected the feed tank to the nozzle and a shielded type-K thermocouple recorded the temperature of the feed solution within the line just ahead of the atomizer. The latter reading was indeed very useful since, in the range of flow rates investigated, the feed solution did boil within the line, despite the high atomizing pressure applied to it. Asbestos tape was therefore carefully pressed around the line and the elbow and that half-inch thick insulation layer was enclosed into a one-inch stainless steel pipe; the feed line was then protected against the large convection and radiation heat fluxes coming from the plasma jet and the feed solution remained at a constant temperature of 10°C within the nozzle throughout the experiments.

#### v. The Condensation-Sampling Unit for the Effluent Gas and Solids

The effluent gas and the small particles of dry product discharged at the bottom of the conical section of the chamber into a one-inch stainless steel pipe (see Figure 9). A shielded type-K thermocouple continuously recorded the gas outlet temperature, which is always of great importance in the control process of a spray drying operation.

The superheated gas mixture consisting of steam, nitrogen and very small entrained particles of salt was directed through a

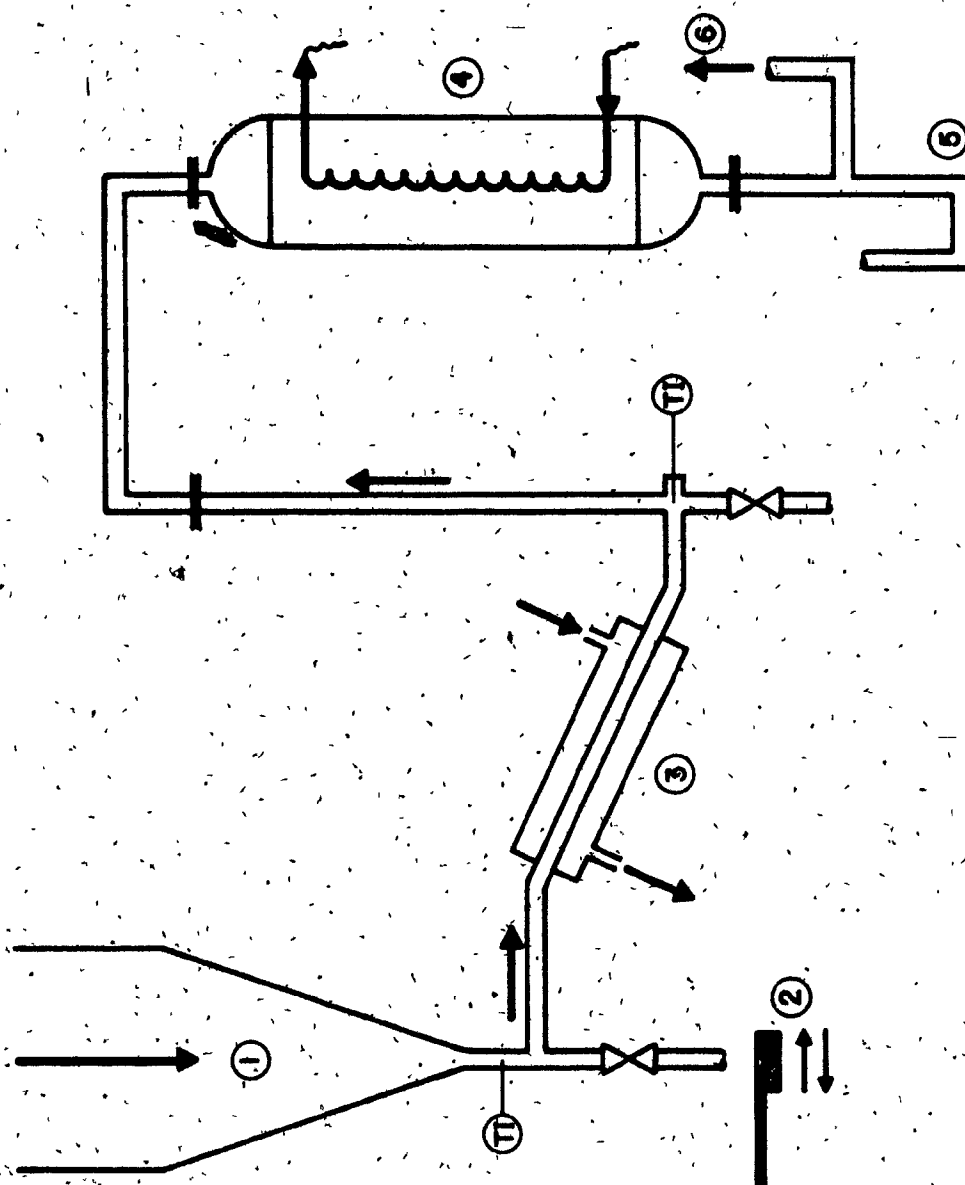
Figure 9

CONDENSATION AND SAMPLING SYSTEM

FOR THE EFFLUENT GAS



- ① SPRAY- DRYING CHAMBER  
 ② SAMPLING CELL AND HOLDER  
 ③ CONDENSER (OPTIONAL)  
 ④ GLASS CONDENSER  
 ⑤ WATER TRAP  
 ⑥ VENT



side port to a double-pipe stainless steel heat exchanger which was operated so that the gases entered the pyrex coil-type condenser at a temperature lower than  $150^{\circ}\text{C}$ . The latter was 0.15 m in diameter and 0.84-m long, with a heat transfer area of  $1.5\text{ m}^2$ ; the uncondensable nitrogen was finally vented to the atmosphere. Even the largest particles of product were entrained within the gas stream up to the condenser, where they redissolved in the condensed steam. Since the aim of the work was to test the drying operation itself, no product recovery equipment - such as cyclones and bag-filters - was provided in the exhaust system. However, samples of product were taken by opening the valve at the bottom of the chamber, in order to see if the particles of product were dry, whether they were thermally degraded and what their shape was. The sampling cells were cylindrical, with an inside diameter of  $1/2$  in (1.27 cm), an outside diameter of  $19/32$  in (1.51 cm) and a length of  $1/4$  in (0.635 cm); they were prepared and examined according to the method described later on for the measurement of the initial drop size distribution of the spray.

#### PROCEDURE

The objective of the experimental work was essentially to determine the maximum evaporative capacity of the drying chamber and its corresponding thermal efficiency, at steady-state, under a set of fixed operating conditions and for several initial concentrations of the feed solution. The procedure consisted of first igniting the

plasma torch and adjusting it to the desired operating conditions. Atomization of the feed was started immediately following the plasma torch ignition to avoid overheating of the top part of the chamber and of the atomizing system. Once steady state was attained, the maximum drying capacity was determined by gradually decreasing the feed rate until the samples collected at the bottom of the chamber were dry.

Some details of the procedure are worth noting: for ignition, argon was fed to the torch at a flow rate of 25 L/min measured at ambient atmospheric conditions. Once a spark was struck, the power supplied to the torch was immediately increased in order to obtain a stable arc at about 300 A and 22 V. The plasmagen gas was then changed to nitrogen by slowly increasing the nitrogen flow rate while decreasing that of argon until the torch operated on a pure nitrogen flow rate of 22.7 L/min (1.59 kg/h). During this gas substitution, the arc voltage slowly increased to a final value of 66 volts at 300 A resulting in a much more energetic plasma jet. At this point, injection of low-temperature steam within the plasma column was started. The steam generated at atmospheric pressure at a rate of 1.25 kg/h by the small Presto cooker, was superheated to 106°C before its introduction into the anode. The injection of steam within the plasma jet did not affect significantly the electrical characteristics of the pure nitrogen arc; it is believed that the small amount of steam introduced at the exit of the nozzle below the arc-striking path did not affect the position of the arc.

root and consequently did not modify the arc voltage. The power supplied to the torch was finally set at its fixed operating value of  $395 \pm 5$  amperes and  $64 \pm 2$  volts, which was used throughout the experimental program. The stable plasma jet, composed of a mixture of highly superheated steam and nitrogen (56% by mass) emitted considerable noise due to turbulence and intense visible radiation, alternatively pink and white in colour.

The flow rate of cooling water through the torch, set at a fixed reading of the rotameter was carefully metered during the experimental run while its inlet and outlet temperatures were continuously recorded. The data were required to calculate the efficiency of the torch.

A heating time of 45 minutes had to be allowed before starting the experiment proper. For the latter, the feed flow rate was set at a maximum value which was fixed before the experiment by adjusting the pump piston stroke. This feed rate was then progressively decreased by gradually closing the needle valve before the rotameter until the optimum feed rate was found; a good indication of the latter was first given by the gas outlet temperature which increased significantly once all the water in the feed was completely vaporized. Another indication was given when the bottom valve on the reactor was opened to the atmosphere. At too high a feed rate, semi-wet deposits and even liquid were observed; at too low a feed rate, overheated particles of salt melted and even plugged the exit

of the chamber while the outlet temperature increased considerably.

For each feed flow rate, a standard procedure was used: as soon as the feed rate had been set at a new value, about 15 to 20 minutes were allowed to reach the new pseudo-steady state within the system, which was indicated by the relative steadiness of the temperature profiles recorded. During that time the main system variables such as the power supplied to the torch and the feed flow rate were carefully monitored and recorded every 2 minutes and the gas outlet temperature was checked to detect plugging. As soon as pseudo-steady state was reached, the bottom valve of the chamber was opened, the outlet gases were allowed to exit freely for 2 minutes, and then three cells were successively passed through the gas stream along three different horizontal directions, at a distance of about 3 centimeters below the valve exit. These cells were immediately examined under a Nikon microscope with a 100 x magnification and dia-illumination, to determine whether the product particles were completely dried or not. Following the sampling, all the system variables were once more carefully recorded.

As soon as the maximum evaporative capacity of the chamber had been determined for a given concentration of the feed, the initial drop size distribution of the spray generated under the same atomizing conditions, but in a cold air ambient medium instead of the superheated steam atmosphere, was investigated according to the immersion sampling technique described in the next section.

### MEASUREMENTS OF THE INITIAL DROP SIZE DISTRIBUTION OF THE SPRAY

An accurate knowledge of the initial drop size distribution of the spray is a prerequisite for a fundamental and meaningful analysis of the system since any calculation of the evaporation rate requires a detailed knowledge of the distribution of droplets sizes; as it was intended to carry out a mathematical prediction of the operation, (see Part II) an experimental determination of the initial drop size distribution was essential.

A very comprehensive review on spray drop size determination can be found in the thesis of Baltas (1967), whereas a more recent one is presented in a paper by Azzopardi (1979); in the latter, the author indicates the principles, the optimum range of sizes and the difficulties and errors encountered in each method, but he does not give any simple general recommendations.

Of the many methods described in these two papers, the one found the most suitable for the present work was the immersion sampling technique, in which the spray droplets are caught in an immiscible fluid of lower density, photographed, measured and counted. It was first described by Rupe (1949) and more recently by Baltas (1967). The chief drawback of immersion sampling is that it discriminates in favor of the largest drops; that can be reduced by making the cells as small as it is technically possible, while keeping the ratio of the cell diameter to the largest expected

droplet diameter above 50. The cells were thus built in the shape of a cube, about 6.4 mm (1/4 in) on the side, with walls made out of a very thin (0.18 mm) brass foil; the bottom of the cells was made of glass microscope slides cut to 6.4 mm (1/4 in) square pieces which were fixed to the walls with epoxy cement. The cell holder - a brass bar 6.4-mm wide (1/4 in) and 12.7-mm (1/2 in) high - was designed to handle four cells along a radius of the reactor, so that a mean distribution across the whole spray pattern could be obtained. The cell holder was welded at the center of a hollow cylinder, made out of brass, 5.16 cm in diameter and 11.90-cm long, tightly fitting in the flanged ports of the chamber. The cells were located at 2.70 cm (1.06 in), 6.0 cm (2.37 in), 9.7 cm (3.80 in) and 13.5 cm (5.30 in) respectively from the center of the spray pattern, according to Baltas' design. As the drops collected in each cell were summed directly to obtain one mean distribution, the cells had to be exposed to the spray for different time intervals corresponding to the annular areas of the dryer cross-section that they represented. The difference in exposure times was achieved by means of a sliding cover presenting a specific opening for each cell, the widths of these four openings being respectively 0.90 mm (1/28 in), 2.70 mm (3/28 in), 4.50 mm (5/28 in) and 6.35 mm (7/28 in), from the center to the edge of the spray. The openings were actually made 3/28, 3/28, 5/28 and 7/28 in wide in order to provide a more favourable ratio of opening width to largest expected droplet diameter in the first cell and the number of drops considered in that cell was then divided by 3 to get

the proper mean sample for the overall distribution. That modification is not essential since the largest droplets generated by atomization are generally located at the edge of the spray pattern rather than at its center and the number of large droplets within that first cell was therefore not likely to be much underestimated. A second protective shield was provided so that the sampling device remained dry until it was correctly positioned within the spray. Figure 10 is a photograph of the disassembled sampling device, showing clearly the four cells, the holder and the two sliding covers.

The properties of the immersion liquid relative to the sprayed solution are important: it must be immiscible, with a lower density so that the drops remain submerged under the surface of the medium. It must also preserve the sphericity of the droplets and prevent their coalescence or evaporation. Accordingly, a hydrocarbon mixture commercially known as Varsol, having a specific gravity of 0.77, was selected.

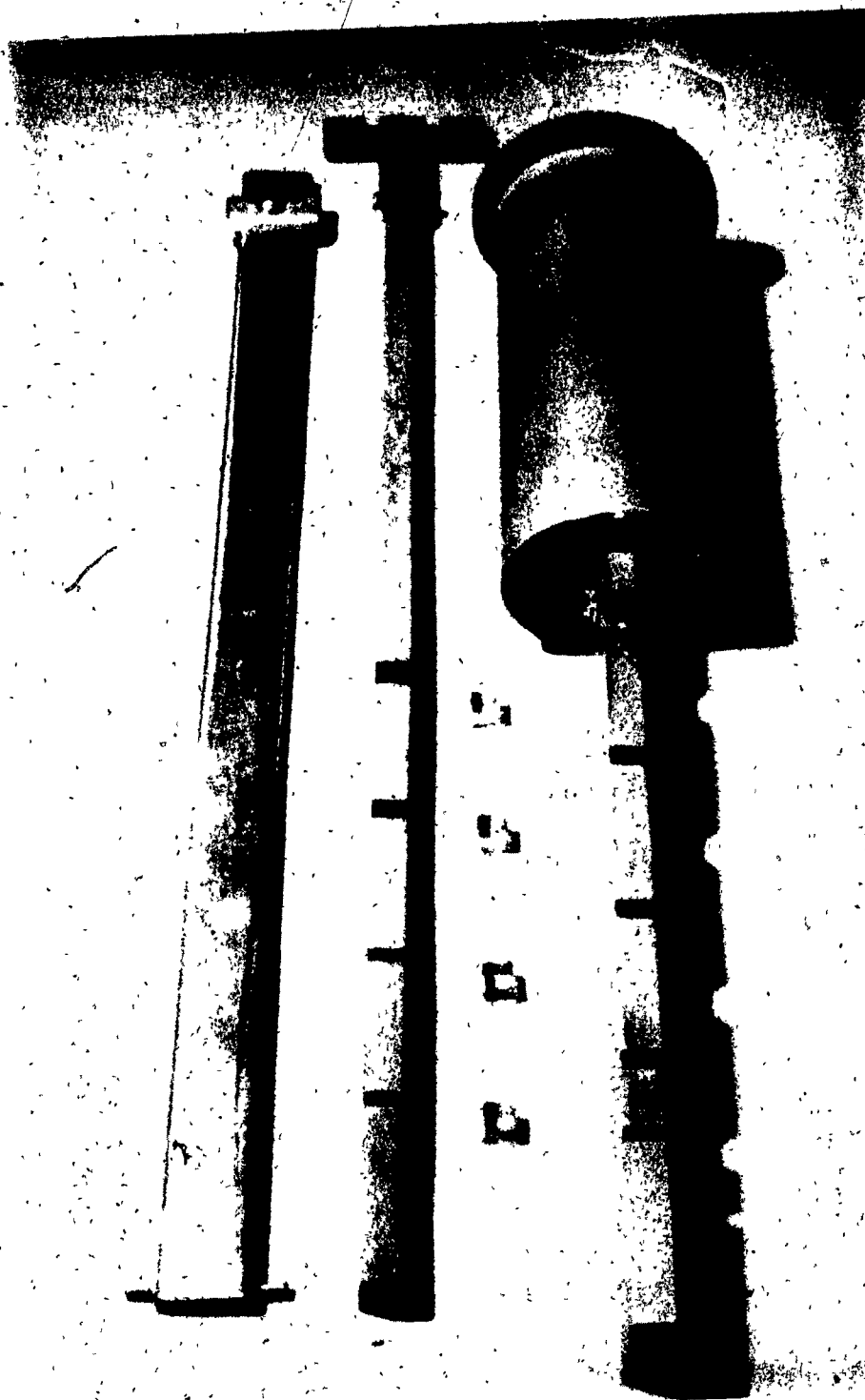
The drop size distribution was determined according to the following standardized procedure:

The individual cells were carefully cleaned, and the glass surface was then treated with a dimethyldichlorosilane solution about 2% in 1, 1, 1 - trichloroethane, which gives water-repellent properties to glassware; that treatment was carried out to prevent the flattening and distortion of the water droplets on the bottom glass side, which occurred in its absence because of the large density



FIGURE 10

PHOTOGRAPH OF DISASSEMBLED SAMPLING DEVICE



difference between drops and Varsol. The cells were then dried, filled completely with Varsol and put on the holder. The holder with cells presented the same configuration to the flow as the original solid bar for which impaction efficiencies of about 100% had been calculated by Baltas with unevaporated droplets larger than 20  $\mu\text{m}$ . The spray was set within the chamber at the atomizing conditions corresponding to the maximum evaporative capacity determined for the feed concentration studied and collection was made in ambient room saturated air. As soon as the sampling device was correctly positioned within the spray at a distance of 10 centimeters from the nozzle exit, the second shield was withdrawn and the sampling was achieved by quickly sliding the main shield forth and back along the cell holder in order to expose briefly the cells to the spray.

Following sample collection, the cells were carefully removed from the holder and brought under the Nikon Measuroscope. A Polaroid Land Instrument Camera, model ED 10, was used to take ten photographs along the outer edges of each cell under a magnification of 100 x and dia-illumination. The droplets were counted and measured with the micrometric scale which could be seen on the prints; that scale, carved on a reticle placed inside one of the eye pieces of the microscope was carefully recalibrated against a stage micrometer of 2 mm graduated in 0.001-mm increments.

The droplets and their size were then treated by computer to obtain the number relative and cumulative frequency curves and to make a partition of 12 to 13 classes.

### Torch Operating Characteristics

The plasma torch was operated under the following set of conditions throughout the experiments, since these appeared to be very well suited to prolonged operation:

|                               |   |             |
|-------------------------------|---|-------------|
| Nitrogen Plasma Gas Flow Rate | : | 1.59 kg/h   |
| Superheated Steam Flow Rate   | : | 1.25 kg/h   |
| Arc Current                   | : | 390 - 400 A |
| Arc Voltage                   | : | 62 - 66 V   |

The torch nozzle diameter was 0.794 cm (5/16 in). The arc voltage and current exhibited small fluctuations from one experiment to another or even during a single experiment, due to either small variations of the gas flow rates or anode erosion, both causing disturbances in the arc striking path between the electrodes.

An energy balance performed on the torch showed that about 50% of the power fed to the torch was actually lost to the cooling circuit of the torch; in the determination of the "effective power" really transferred to the jet, account was also taken of the power dissipated in the electrical cables, since the potential drop in the cables between the console and the torch was as high as 3 volts; the effective power transferred to the plasma gas was then typically about 12.9 kW, the specific enthalpy corresponding to the  $H_2$ /steam mixture being  $1.6 \cdot 10^7$  J/kg. The precision of that variable calculated from four experimentally-read values was found in any

case close to 7.5%, by considering the experimental errors introduced in the reading of these values.

A detailed calculation of the effective power and its precision in a typical run is performed in Appendix A; a table of torch operating characteristics recorded during one experiment is also included in that appendix. The net power effectively transferred to the plasmagen gas and the mass flow rate of gas, together with the available thermodynamic tables computed respectively by Sewell and Fenter (1962) and Ihara (1977) for high-temperature nitrogen and steam at one atmosphere, were used to yield the mean values of plasma enthalpy, velocity and temperature at the anode nozzle exit for the typical value of the effective power given above. These calculations are also included in Appendix A. The plasmagen gas, composed of nitrogen (56% by mass) and steam (44% by mass), exited from the anode-nozzle at a mean velocity of 300 m/s, a mean temperature of 3 580 K and its specific enthalpy was  $1.6 \cdot 10^7$  J/kg; these values are somewhat lower than those corresponding to a pure nitrogen plasma flame under the same operating conditions and this is essentially due to the dilution of the plasma column within the nozzle with low-temperature steam.

### Heat and Mass Balances

Heat and mass balances have to be worked out to assess the dryer performance from the data experimentally obtained.

It is assumed that the operation is continuous with negligible hold-up of the product on the chamber walls; with the symbolism shown in Figure 11, the overall heat and mass balances on the chamber are as follows:

$$P_{out} + W_G H_{(T_{Gin})} + W_F H_F = W_{T_o} H_{T_o} + q_L \quad (1)$$

and

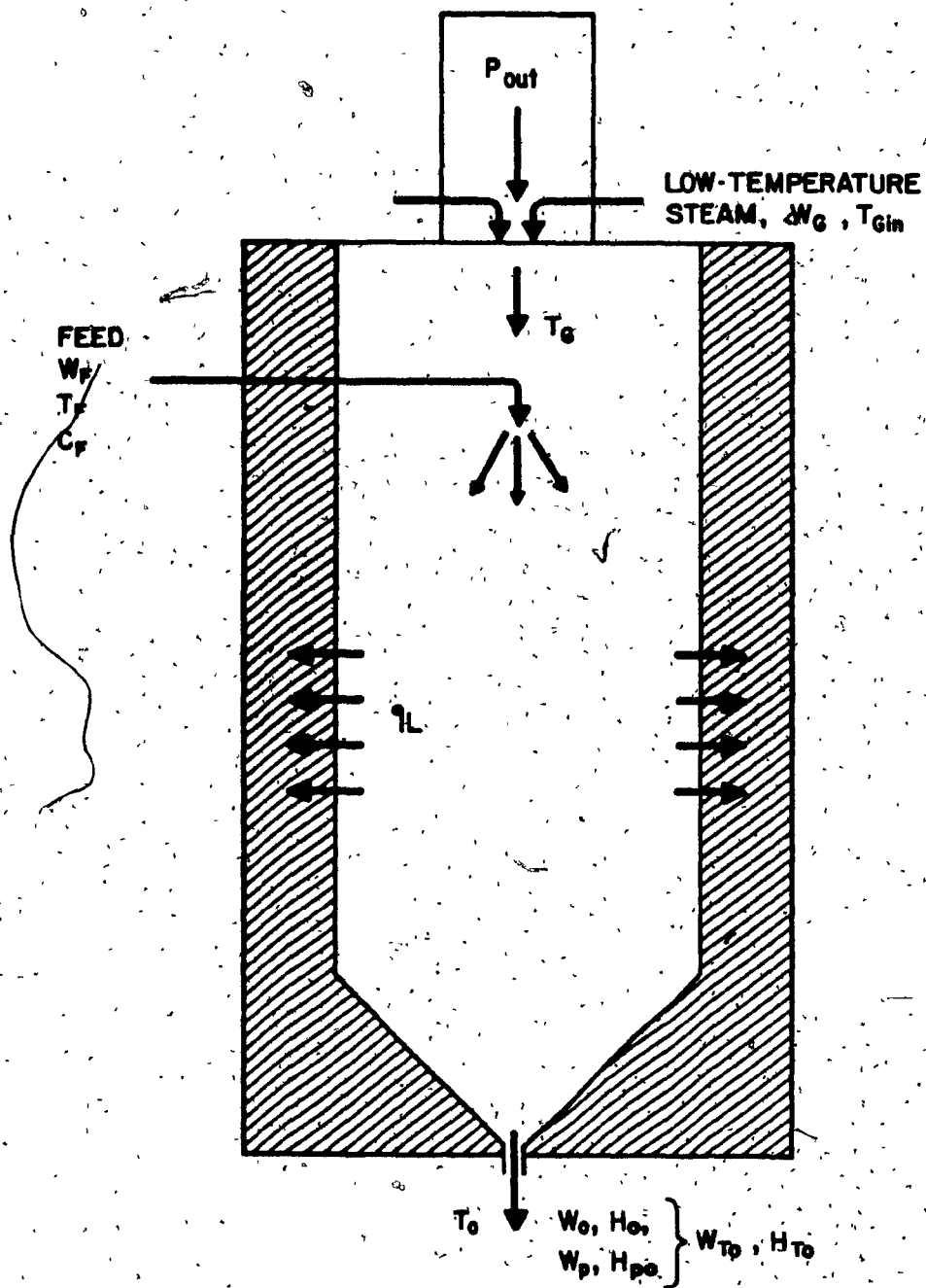
$$W_{T_o} = W_G + W_F \quad (2)$$

The presence of nitrogen is ignored, because of its very low heat capacity compared to that of steam at the same temperature.

In the above equations, all the variables, but two, are directly or indirectly known from the experimental measurements; the two unknowns are the exact composition of the outlet mixture which is made up of steam, solid product and residual moisture when not completely dried, and the total amount of heat losses  $q_L$  through the dryer walls.

Since the objective is to obtain completely dried product at the exit of the dryer, the following heat and mass balances can be written, leading to the value of  $q_L$ , for zero moisture content in the exiting particles:

FIGURE 11SYMBOLS USED IN HEAT AND MASS BALANCES





$$W_p = W_F C_F \quad (3)$$

$$W_o = W_G + W_F (1 - C_F) \quad (4)$$

$$P_{out} + W_G H(T_{Gin}) + W_F H_F = (W_p H_{p_o} + W_o H_o) + q_L \quad (5)$$

When a product particle reaches zero residual moisture content, it is assumed to heat up instantaneously to the steam outlet temperature or at most to its melting point temperature, if the steam outlet temperature is higher, so that  $H_{p_o}$  is known. Such an assumption cannot be made if the feed rates exceed the maximum capacity of the chamber since the residual moisture content within the particles is then unknown. From now on, the total energy used to dry and to heat up the components of the feed (water and solid product) to the dryer outlet temperature must be distinguished from the total energy used in the chamber, since a small fraction of the latter is taken by the low-temperature steam injected in the nitrogen plasma flame. The total energy used in the chamber is given by

$$(W_p H_{p_o} + W_o H_o) - (W_F H_F + W_G H(T_{Gin})) = (P_{out} - q_L) \quad (6)$$

It permits the calculation of the total heat losses through the walls by difference with the effective energy  $P_{out}$  contained in the plasma flame. On the other hand, the heat effectively transferred to the feed components is equal to:

$$[W_p H_{p_o} + W_F (1 - C_F) H_o] - W_F H_F = \Delta H \quad (7)$$

This term is more interesting from a general point of view, as it corresponds to the operation under the same operating conditions but with a pure water vapor plasma torch.

The dryer performance is clearly represented by the drying efficiency defined as the fraction of total heat supplied to the dryer recovered in the exit steam at the bottom of the chamber. The whole basis for an economic operation is the utilization of the heat delivered into the dryer; since the exit steam at the bottom of the chamber is highly superheated, all the energy contained in the latter can be successfully recovered by simple condensation in fairly sophisticated heat exchangers (Foxall and Chappell, 1980); that energy includes not only the heat used in the evaporation and drying process, but also the heat given to increase the temperature of the exit gas which is always lost in conventional air dryers; it results in a much more interesting operation as seen later, provided that that energy is really efficiently recovered.

Assuming that the total amount of atmospheric steam going out of the dryer can be condensed into water at 20°C after the solid product has been removed, the drying efficiency for the actual operation is:

$$\eta_1 = \frac{W_o [H_o - H_{(water, 20^\circ C)}]}{P_{out} + P_{atomization} + W_F H_F + W_G H_{(T_{Gin})}} \quad (8)$$

The denominator is the total energy supplied to the dryer and consists of (1) the effective energy of the plasma flame, (2) the energy required to atomize the feed solution at a high pressure, (3) the enthalpy brought by the feed solution at  $T_F$ , and (4) the low-temperature steam injected into the flame; of these four terms  $P_{out}$  is the most important;  $P_{atomization}$  is approximately given by the electrical energy consumed by the pump, assuming an efficiency of unity; since the pump consumed 6 A at 110 volts, the energy used in the atomization process was then roughly 0.7 kW which is about 5% of  $P_{out}$ ; the other two terms are even smaller and they may be neglected with confidence, compared to  $P_{out}$ .

Another drying efficiency can be defined for the operation with a pure water vapor torch giving the same effective power  $P_{out}$  under the same otherwise operating conditions:

$$\eta_2 = \frac{W_F (1 - C_F) [H_o - H_{(water, 20^\circ C)}]}{P_{out} + P_{atomization} + W_F H_F} \quad (9)$$

It is defined in the same way as  $\eta_1$ , but the terms corresponding to the low-temperature steam injected in the plasma flame have been omitted; it has therefore a more general utility.

Both of these efficiencies do not include the thermal efficiency of the torch which is rather low for a small torch of the type used in this study (about 50%); an overall efficiency of the actual operation can be obtained with Equation (8), by

replacing the torch effective power  $P_{out}$  by the electrical power  $V.I$  fed to the torch.

## RESULTS

### 1. 30% $NaNO_3$ Solution

The results for this feed concentration are reported in Table II. The headings for this Table can be found in the Nomenclature and are:  $\underline{W}_F$ , the feed mass flow rate, in kg/h;  $\underline{p}$ , the atomization gauge pressure, in MPa; the nature of the solid product obtained;  $\underline{T}_O$ , the outlet steam temperature, in °C;  $\underline{\Delta H}$ , the energy transferred to the components of the feed (solid in solution and water) to vapourize the water and dry the particles, in kW;  $\underline{q}_L$ , the heat losses through the walls, in kW and as a % of total energy used;  $\underline{E}$ , the evaporative capacity of the chamber, in kg/h of water evaporated;  $\underline{\eta}_1$  and  $\underline{\eta}_2$ , the two efficiencies, previously defined. It is to be noted that the efficiency of the plasma torch is not considered in the calculation of  $\underline{\eta}_1$  and  $\underline{\eta}_2$ . Had this been done, the overall efficiency of the system would have fallen to 44%. This is not surprising, since small torches are very inefficient. The six columns on the right hand side obviously apply to a dry product only. The method of calculation for  $\underline{P}_{out}$  is outlined in Appendix A. The nature of the product was determined from a careful examination of the three samples collected at the bottom of the chamber. Several typical photographs of the latter are shown in Appendix C.

**TABLE II**  
**RESULTS FOR 30% NaNO<sub>3</sub>**

| Run No. | W <sub>F</sub><br>kg/h | p<br>MPa | P <sub>out</sub><br>kW | Product Nature | T <sub>O</sub><br>°C | ΔH<br>kW | q <sub>L</sub><br>kW | q <sub>L</sub><br>% | E<br>kg/h | η <sub>1</sub><br>% | η <sub>2</sub><br>% |
|---------|------------------------|----------|------------------------|----------------|----------------------|----------|----------------------|---------------------|-----------|---------------------|---------------------|
| A1      | 25.5                   | 1.72     | 12.8                   | WET            | 235                  | -        | -                    | -                   | -         | -                   | -                   |
| A2      | 25.5                   | 1.90     | 13.0                   | WET            | 230                  | -        | -                    | -                   | -         | -                   | -                   |
| A3      | 22                     | 1.38     | 12.8                   | WET            | 235                  | -        | -                    | -                   | -         | -                   | -                   |
| B1      | 18.2                   | 2.07     | 12.65                  | DRY            | 315                  | 11.1     | 1.35                 | 12                  | 12.75     | 79                  | 77                  |
| B2      | 17.7                   | 2.41     | 12.8                   | DRY            | 330                  | 10.9     | 1.70                 | 15                  | 12.40     | 77                  | 75                  |

The number of runs with 30%  $\text{NaNO}_3$  solution were limited, due to difficulties experienced with plugging at this high concentration. It is probable that a feed rate slightly larger than 18.2 kg/h would have been more representative of the maximum flow rate. This is confirmed by the fairly high exit temperature of  $315^\circ\text{C}$ .

Figure 12 shows the experimental temperature profiles of the gas mixture at the centerline of the dryer and of the dryer wall for runs A1, A3 and B1.

Figures 13 and 14 represent the relative frequency and the number cumulative frequency of the initial drop size distribution determined for the atomization pressure of 2.07 Mpa (300 psig), corresponding to the estimated maximum capacity of the chamber. Concerning Figure 13, the statistical average was  $35.264 \mu\text{m}$ , the median  $31.161 \mu\text{m}$ , the variance 598.462 and the standard deviation 24.463.

Table III shows a comparison between experimental and calculated values of the Sauter Mean Diameter,  $D_{VS}$ , and of the largest stable droplet diameter,  $D_{MAX}$ , of the spray. The experimental Sauter Mean Diameter was obtained from the number relative frequency curve by means of the equation:

$$D_{VS} = \frac{\sum_i n_i d_i^3}{\sum_i n_i d_i^2} \quad (10)$$

whereas the "calculated values" were calculated according to

FIGURE 12TEMPERATURE PROFILES FOR30% NaNO<sub>3</sub> SOLUTIONS, ANDMONARCH 4.5 NOZZLE

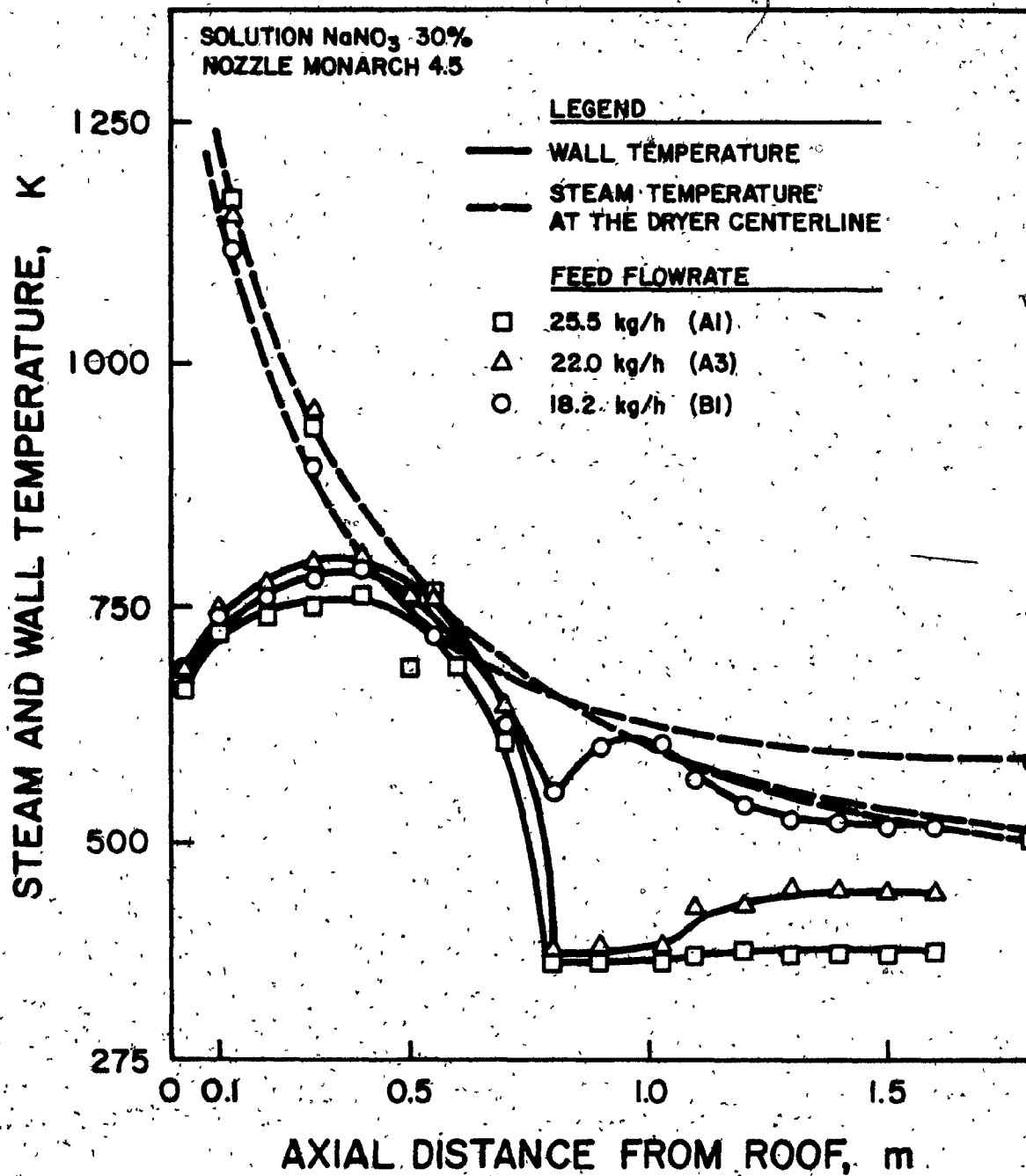




FIGURE 13DROPLETS RELATIVE NUMBER DISTRIBUTION30% NaNO<sub>3</sub> SOLUTION, AND MONARCH4.5 NOZZLE AT A PRESSURE OF 2.07 MPa GAUGE

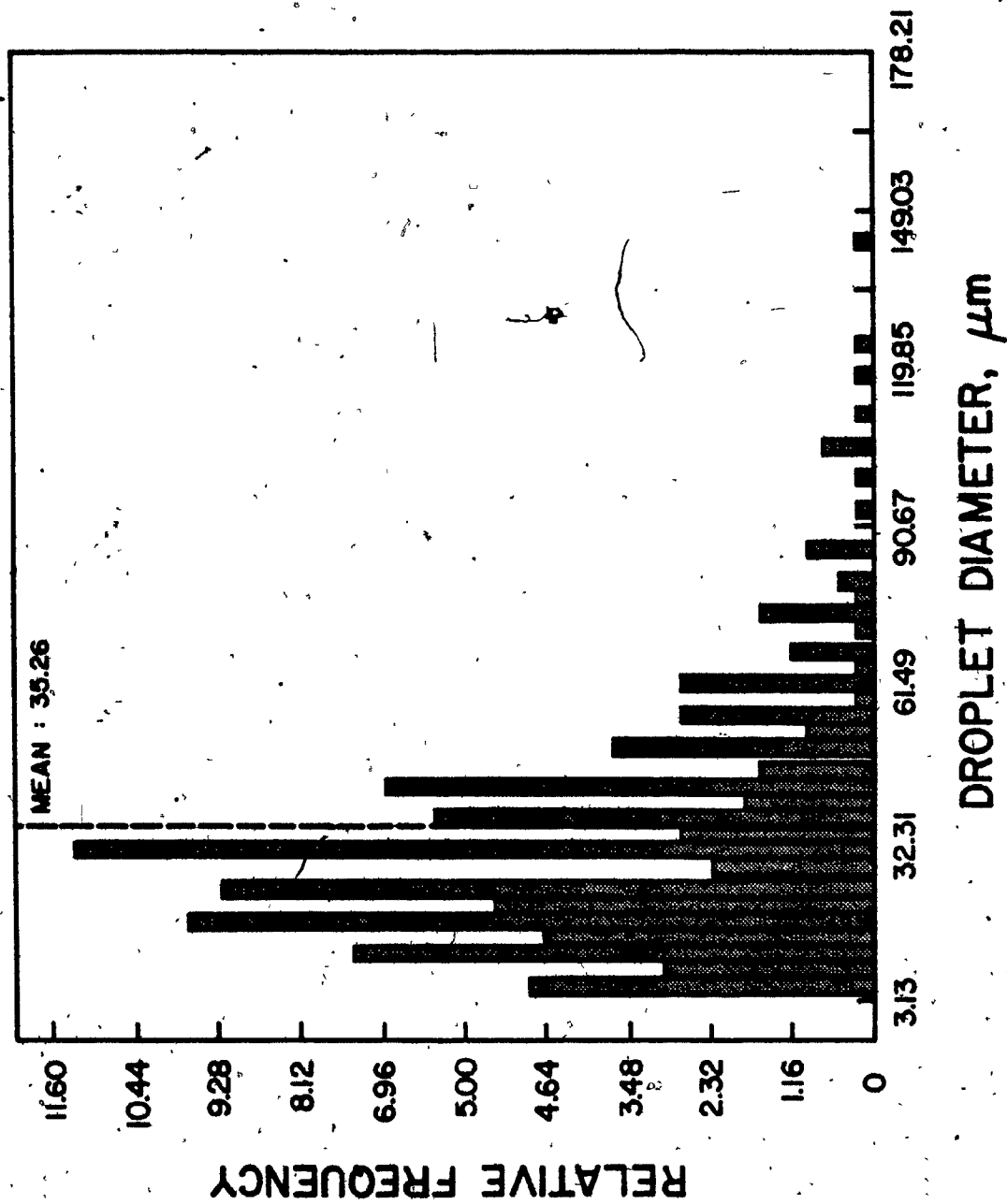


FIGURE 14DROPLETS CUMULATIVE NUMBER DISTRIBUTION30% NaNO<sub>3</sub> SOLUTION, AND MONARCH4.5 NOZZLE AT A PRESSURE OF 2.07 MPa GAUGE

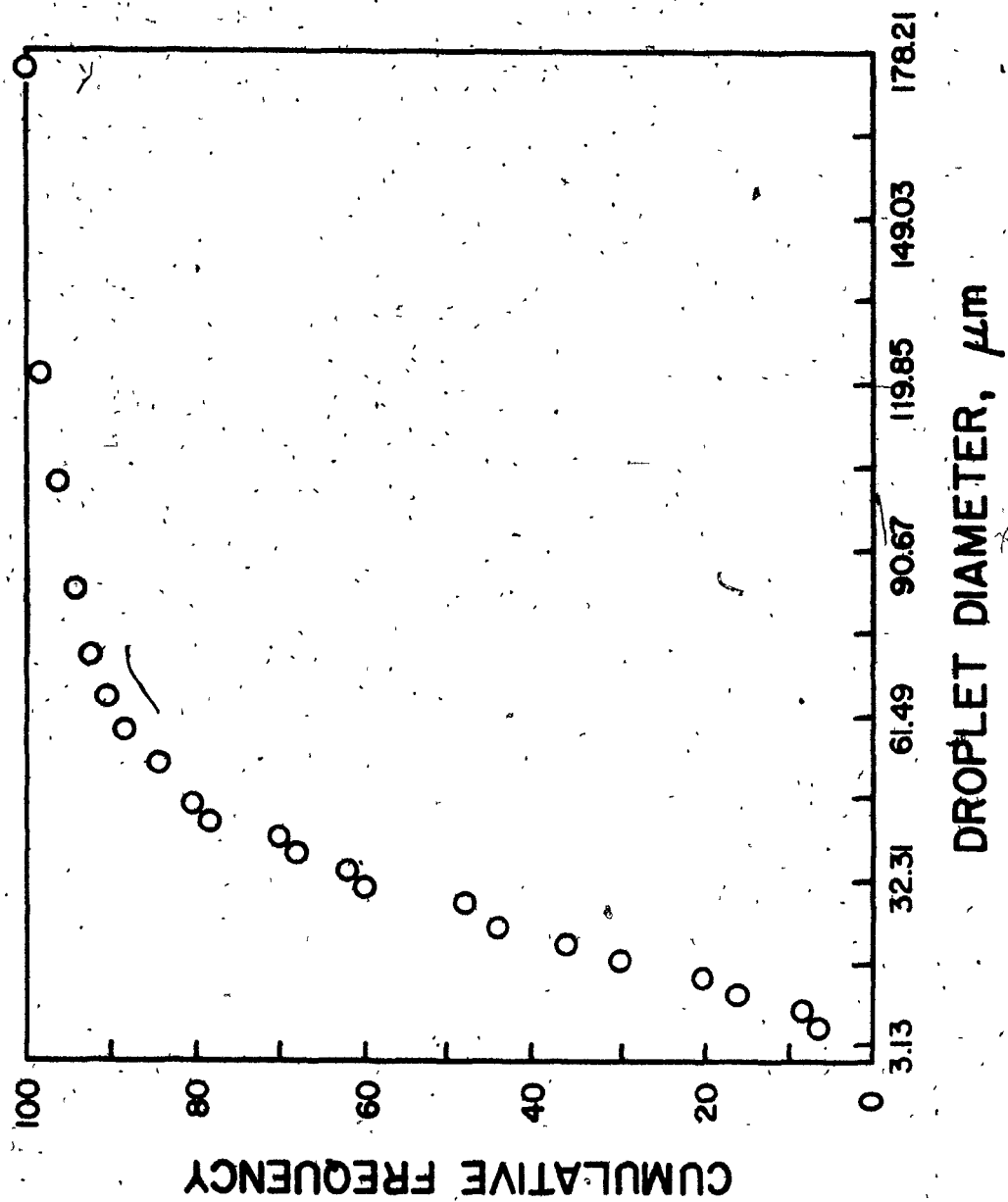


TABLE III

COMPARISON OF EXPERIMENTAL  
AND CALCULATED VALUES OF  $D_{VS}$  AND  $D_{MAX}$   
FOR 30%  $\text{NaNO}_3$  SOLUTION, AND 4.5 MONARCH NOZZLE

|                                 | EXPERIMENTAL VALUE | CALCULATED VALUE | DIFFERENCE |
|---------------------------------|--------------------|------------------|------------|
| Sauter Mean Diameter            |                    |                  |            |
| $D_{VS}$ ( $\mu\text{m}$ )      | 72.2               | 73.2             | 1.4%       |
| Maximum Stable Droplet Diameter |                    |                  |            |
| $D_{MAX}$ ( $\mu\text{m}$ )     | 178                | 176              | 1.1%       |

Mugle and Evans (1951) upper-limit function based on the volume drop size distribution, which generally fits the data from pressure nozzles quite accurately; the details of that calculation are explained in Appendix D. The remarkable agreement between the experimental and calculated values of  $D_{VS}$  and  $D_{MAX}$  (Table III) confirms that the drop size distribution shows a normal trend with no important deformation and a systematic error due to the operating conditions only. Equally worthy of note is the fact that, compared to other feed concentrations, the higher feed density and viscosity of a 30% solution required a net increase in the atomizing pressure, which in turn resulted in a quite fine droplet size distribution, as the small value of the Sauter Mean Diameter indicates.

## 2. 10% NaNO<sub>3</sub> Solution

The optimum feed rate corresponding to the maximum evaporative capacity at 10% NaNO<sub>3</sub> seems to be fairly well approximated by the value of  $15.5 \pm 0.6$  kg/h for an effective power of 12.9 kW (see Table IV). The corresponding outlet temperature at the bottom of the chamber, equal to 310°C, is exactly the melting point temperature of the salt; it is then the highest steam temperature that can be allowed to avoid product quality degradation, since the particles of salt are so small that they certainly reach the temperature of the surrounding steam almost instantaneously. An outlet temperature of 290 to 300°C should be indeed preferred to avoid any product deterioration; it would correspond to a slightly higher maximum evaporative capacity. (The temperature profiles are shown in Figure 15.) However, since 16.7 kg/h gives a wet

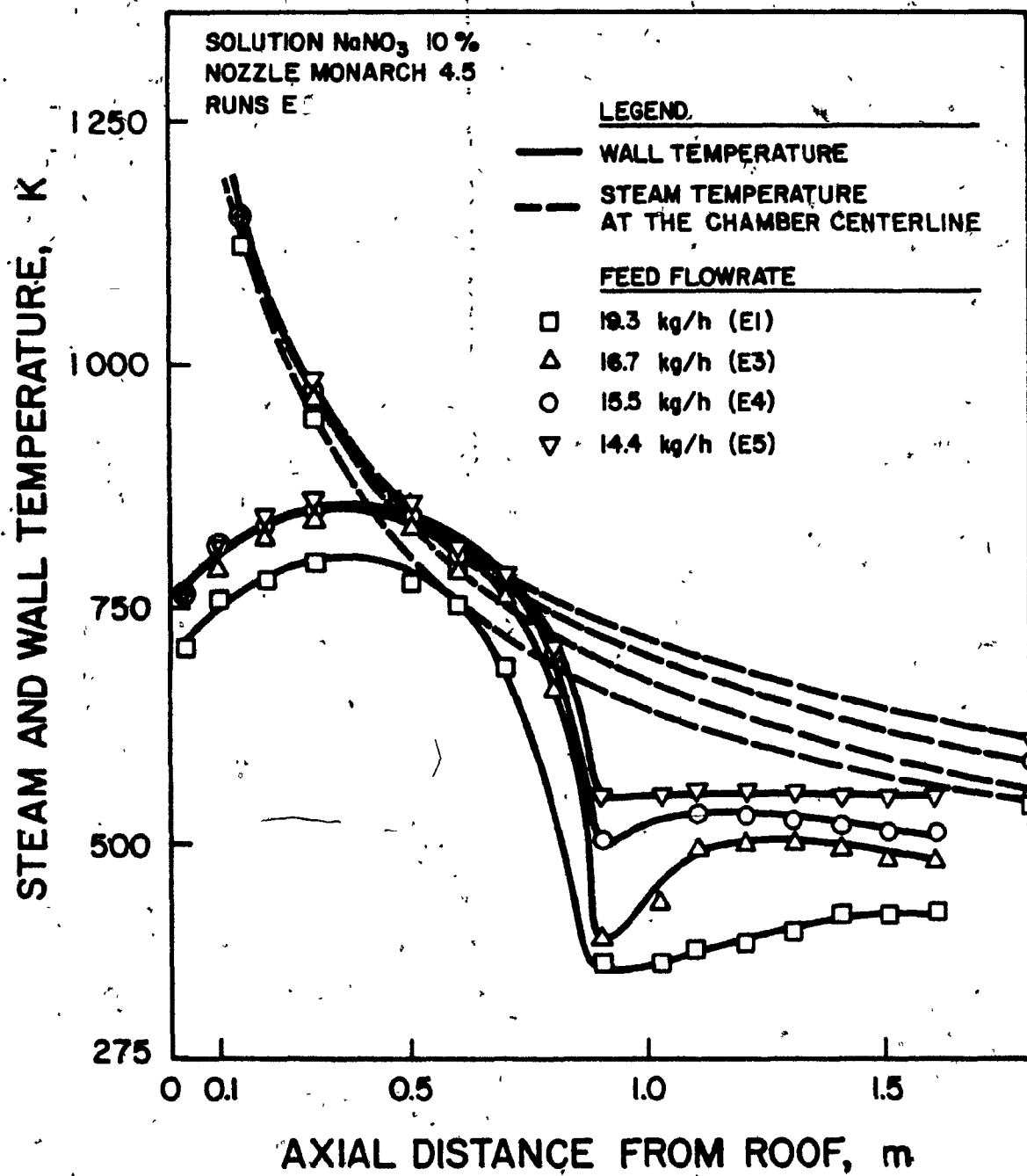
TABLE IV  
RESULTS FOR 10% NaNO<sub>3</sub>

| Run No. | W <sub>F</sub><br>kg/h | p<br>MPa | P <sub>out</sub><br>kW | Product Nature | T <sub>o</sub><br>°C | ΔH<br>kW | q <sub>L</sub><br>kW | q <sub>L</sub><br>% | E<br>kg/h | η <sub>1</sub><br>% | η <sub>2</sub><br>% |
|---------|------------------------|----------|------------------------|----------------|----------------------|----------|----------------------|---------------------|-----------|---------------------|---------------------|
| C1      | 23                     | 1.85     | 11.6                   | WATER          | 240                  | -        | -                    | -                   | -         | -                   | -                   |
| C2      | 20                     | 1.79     | 12.7                   | WET SALT       | 270                  | -        | -                    | -                   | -         | -                   | -                   |
| D1      | 21.5                   | 1.51     | 11.8                   | WATER          | 230                  | -        | -                    | -                   | -         | -                   | -                   |
| D2      | 20.2                   | 1.44     | 12.1                   | WATER          | 225                  | -        | -                    | -                   | -         | -                   | -                   |
| D3      | 21                     | 1.51     | 12.8                   | WATER          | 220                  | -        | -                    | -                   | -         | -                   | -                   |
| D4      | 19.7                   | 1.44     | 12.4                   | WET SALT       | 265                  | -        | -                    | -                   | -         | -                   | -                   |
| D5      | 20.1                   | 1.44     | 12.8                   | WET            | 260                  | -        | -                    | -                   | -         | -                   | -                   |
| D6      | 19.7                   | 1.44     | 12.5                   | WET            | 265                  | -        | -                    | -                   | -         | -                   | -                   |
| E1      | 19.3                   | 1.31     | 12.9                   | WET            | 265                  | -        | -                    | -                   | -         | -                   | -                   |
| E2      | 18.6                   | 1.20     | 12.9                   | WET            | 235                  | -        | -                    | -                   | -         | -                   | -                   |
| E3      | 16.7                   | 0.96     | 12.9                   | WET            | 280                  | -        | -                    | -                   | -         | -                   | -                   |
| E4      | 15.5                   | 0.85     | 12.9                   | DRY            | 310                  | 11.9     | 0.85                 | 7                   | 14        | 86                  | 84                  |
| E5      | 14.4                   | 0.82     | 12.9                   | DRY            | 335                  | 11.25    | 1.5                  | 13                  | 13        | 82                  | 80                  |

FIGURE 15

TEMPERATURE PROFILES FOR  
10% NaNO<sub>3</sub> SOLUTIONS, AND  
MONARCH 4.5 NOZZLE





product at the exit of the chamber, it can be inferred that  $15.5 \pm 0.6$  kg/h is the optimum feed rate at 10%  $\text{NaNO}_3$ , with a maximum error of 8%. The corresponding atomizing pressure is rather low (0.85 MPa or 125 psig) and forms then a coarse and less stable spray; the calculated value of  $D_{VS}$  is much different from the experimental one (33% difference) although the maximum droplet diameters are fairly close together (only 3%) (Table V); this large difference in  $D_{VS}$  is certainly generated by the important irregularities induced in the drop size distribution (Figures 16 and 17) either by atomization instability or by measurement errors. For Figure 16, the average diameter is  $57.852 \mu\text{m}$ , the median is  $49.947 \mu\text{m}$ , and the standard deviation  $37.503$ .

### 3. Pure Water - Nozzle Monarch

In this case, two important series of runs were successfully performed, giving reproducible results, within the experimental errors. The optimum feed rates - which is equal to the maximum evaporative capacity for pure water - were close for the two series of runs, being respectively  $15 \pm 0.6$  kg/h for an effective power of 13 kW (Run F4) and  $14.8 \pm 0.6$  kg/h at 12.8 kW (Run G2), which corresponds in both cases to an efficiency of 87% for the "true" spray drying operation itself (Table VI). The temperature profiles are shown in Figure 18. In both runs, the heat losses through the walls was rather small, about 5% of the evaporative load, and the outlet steam temperature was 280-290°C, as expected.

**TABLE V**  
**COMPARISON OF EXPERIMENTAL**  
**AND CALCULATED VALUES OF  $D_{VS}$  AND  $D_{MAX}$**   
**FOR 10%  $\text{NaNO}_3$  SOLUTION, AND 4.5 MONARCH NOZZLE**

|                                                                   | EXPERIMENTAL VALUE | CALCULATED VALUE | DIFFERENCE |
|-------------------------------------------------------------------|--------------------|------------------|------------|
| Sauter Mean Diameter<br>$D_{VS}$ ( $\mu\text{m}$ )                | 150                | 101              | 33%        |
| Maximum Stable Droplet<br>Diameter<br>$D_{MAX}$ ( $\mu\text{m}$ ) | 219                | 226              | 4%         |

FIGURE 16DROPLETS RELATIVE NUMBER DISTRIBUTION10% NaNO<sub>3</sub> SOLUTION, AND MONARCH4.5 NOZZLE, AT A PRESSURE OF 0.85 MPa GAUGE

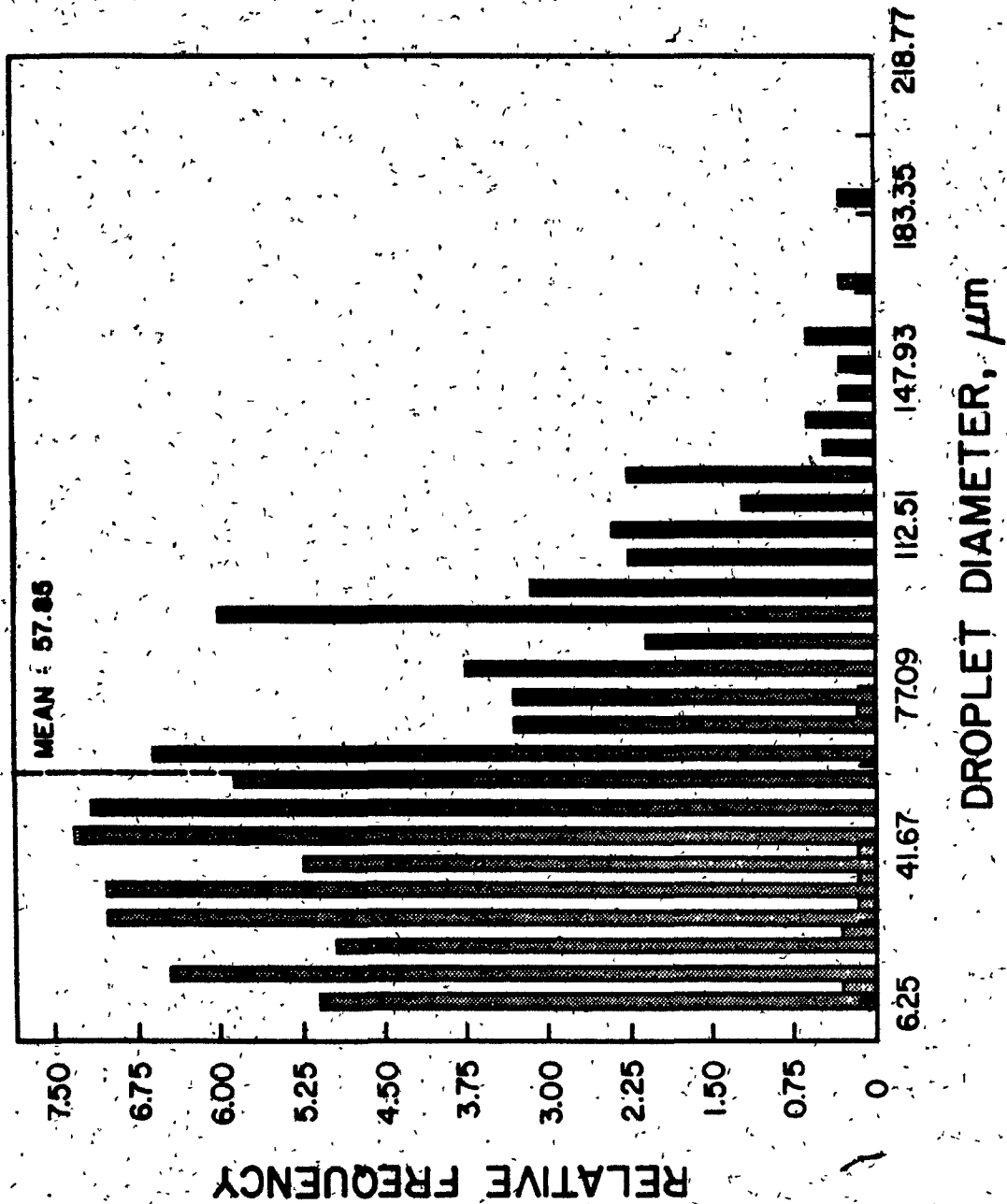


FIGURE 17DROPLETS CUMULATIVE NUMBER DISTRIBUTION10% NaNO<sub>3</sub> SOLUTION, AND MONARCH4.5 NOZZLE AT A PRESSURE OF 0.85 MPa GAUGE

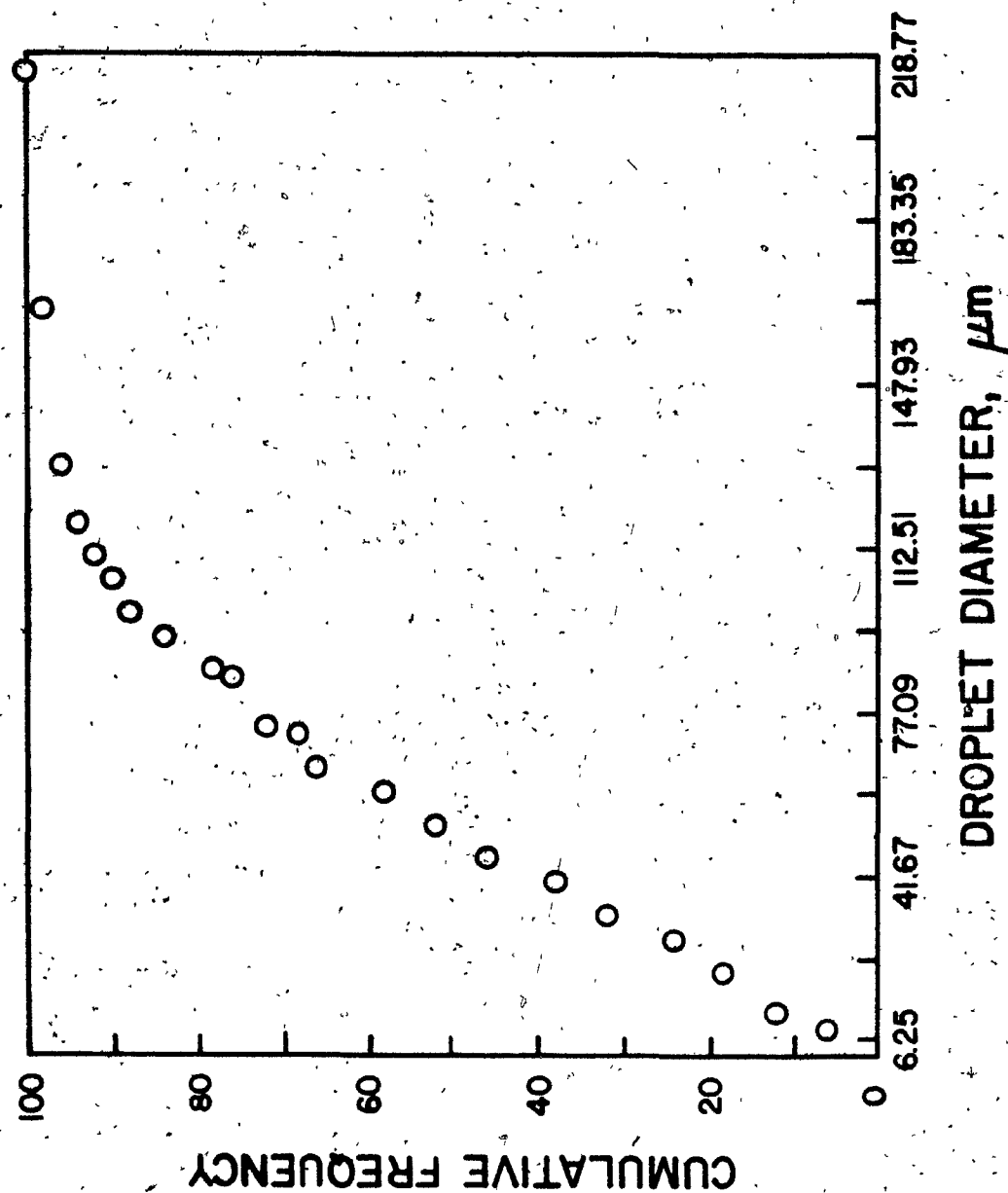


TABLE VI

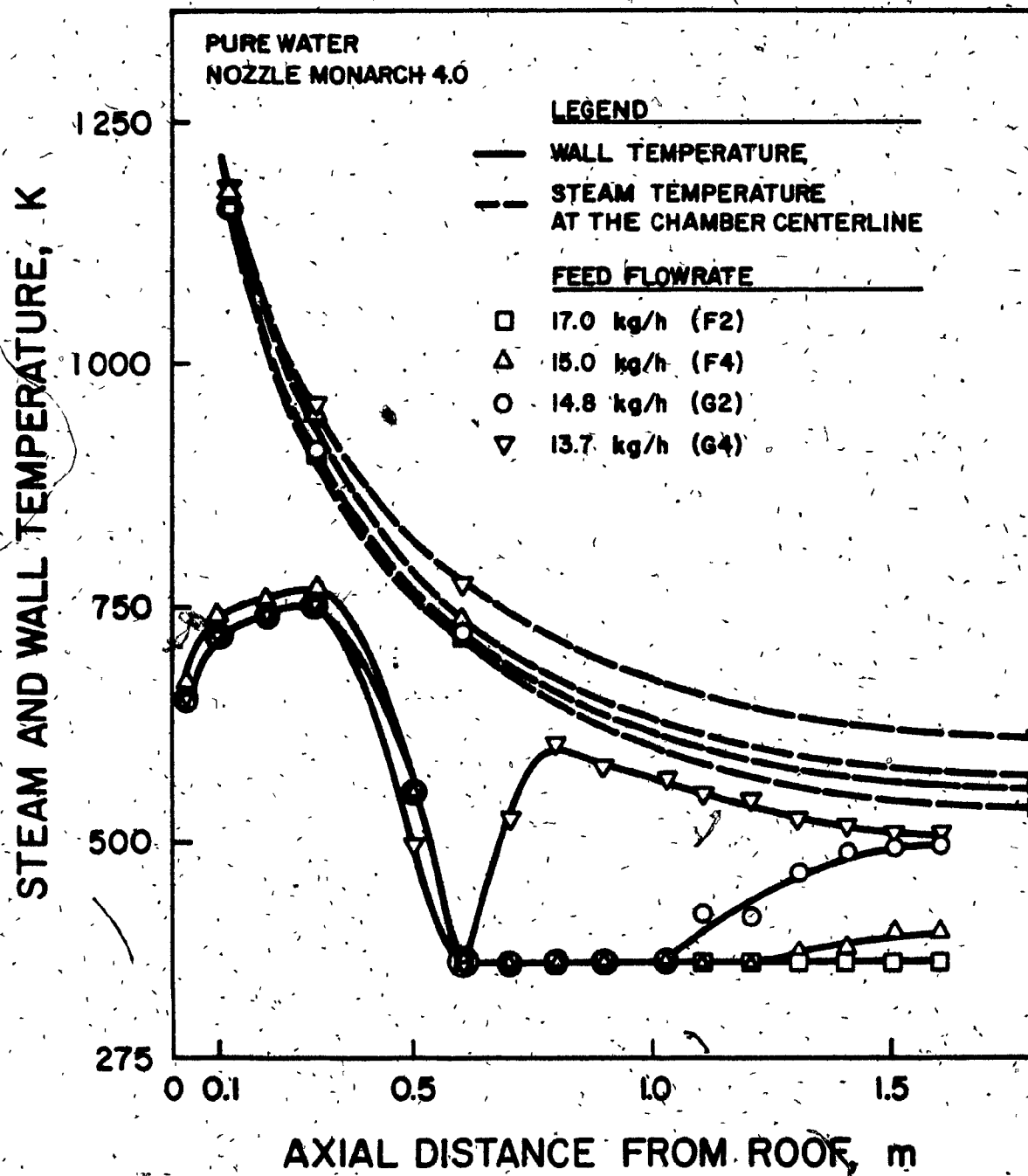
## RESULTS FOR PURE WATER, NOZZLE MONARCH

| Run No. | $W_F$<br>kg/h | $p$<br>MPa | $P_{out}$<br>kW | Product Nature              | $T_o$<br>°C | $\Delta H$<br>kW | $q_L$<br>kW | $q_L$<br>% | $E$<br>kg/h | $\eta_1$<br>% | $\eta_2$<br>% |
|---------|---------------|------------|-----------------|-----------------------------|-------------|------------------|-------------|------------|-------------|---------------|---------------|
| F1      | 16.8          | 1.27       | 13              | WET<br>(dispersed droplets) | 260         | -                | -           | -          | -           | -             | -             |
| F2      | 17            | 1.31       | 13.1            | WET                         | 260         | -                | -           | -          | -           | -             | -             |
| F3      | 16.3          | 1.20       | 13.1            | WET                         | 265         | -                | -           | -          | -           | -             | -             |
| F4      | 15            | 1.10       | 13.0            | DRY                         | 290         | 12.3             | 0.6         | 5          | 15          | 89            | 87            |
| G1      | 16.4          | 1.20       | 12.8            | WET                         | 230         | -                | -           | -          | -           | -             | -             |
| G2      | 14.8          | 1.03       | 12.8            | DRY                         | 280         | 12.0             | 0.65        | 5          | 14.8        | 88            | 87            |
| G3      | 13.8          | 0.93       | 12.8            | DRY                         | 325         | 11.6             | 1.1         | 9          | 13.8        | 86            | 84            |
| G4      | 13.7          | 0.89       | 13              | DRY                         | 335         | 11.6             | 1.3         | 11         | 13.7        | 84.5          | 83            |



FIGURE 18

TEMPERATURE PROFILES FOR  
PURE WATER AND MONARCH 4.0 NOZZLE



As some small water droplets were dispersed within the samples corresponding to feed rates of 16.3 kg/h and 16.4 kg/h, the maximum error on the maximum evaporative capacity, taken equal to  $14.8 \pm 0.6$  kg/h, was about 10%.

The initial drop size distribution at the operating pressure of 150 psig was relatively coarse (Table VII). The number frequency curves (Figures 19 and 20) present still some small irregularities, as those detected for 10%  $\text{NaNO}_3$ , but the resulting experimental and calculated values of  $D_{VS}$  and  $D_{MAX}$  are relatively close together, allowing then more confidence in the distribution. For Figure 19, the statistical average was 42.144  $\mu\text{m}$ , the median 25.474 and the standard deviation 38.219.

#### 4. Pure Water - Nozzle LNN2

The maximum evaporative capacity of the chamber in this case was  $14.5 \pm 0.6$  kg/h for an effective power of 12.8 kW (Run H3), which corresponded again to an efficiency of 86% for the "true" spray drying operation. That maximum capacity was approximated with a maximum error of 10% in this series of runs also (Table VIII). Figure 21 shows the temperature profiles for the H series of runs.

The set of results I is presented in order to illustrate the tremendous effect of over-heating the gas mixture when the feed rate is decreased to much too low a value. Figure 22 shows the temperature profiles; clearly, the profiles of Run I1 ( $14 \pm 0.6$  kg/h) are

TABLE VII  
COMPARISON OF EXPERIMENTAL  
AND CALCULATED VALUES OF  $D_{VS}$  AND  $D_{MAX}$   
FOR PURE WATER, NOZZLE MONARCH 4.0

|                                 | EXPERIMENTAL VALUE | CALCULATED VALUE | DIFFERENCE |
|---------------------------------|--------------------|------------------|------------|
| Sauter Mean Diameter            |                    |                  |            |
| $D_{VS}$ (μm)                   | 109                | 110              | 17         |
| Maximum Stable Droplet Diameter |                    |                  |            |
| $D_{MAX}$ (μm)                  | 238                | 255              | 77         |

FIGURE 19DROPLETS RELATIVE NUMBER DISTRIBUTIONPURE WATER AND MONARCH 4.0 NOZZLEAT A PRESSURE OF 1.03 MPa GAUGE

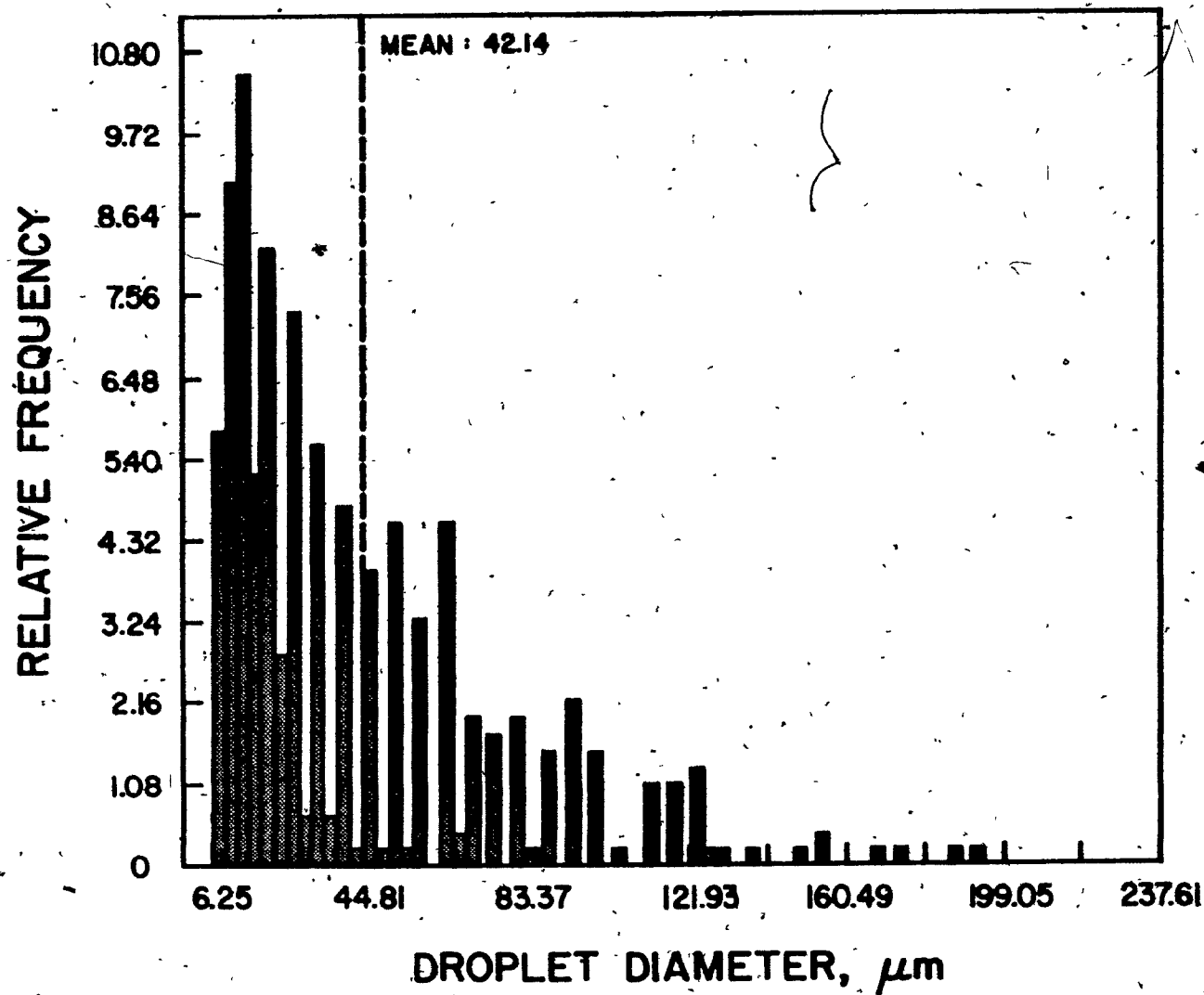


FIGURE 20DROPLETS' CUMULATIVE NUMBER DISTRIBUTIONPURE WATER AND MONARCH 4.0 NOZZLEAT A PRESSURE OF 1.03 MPa GAUGE

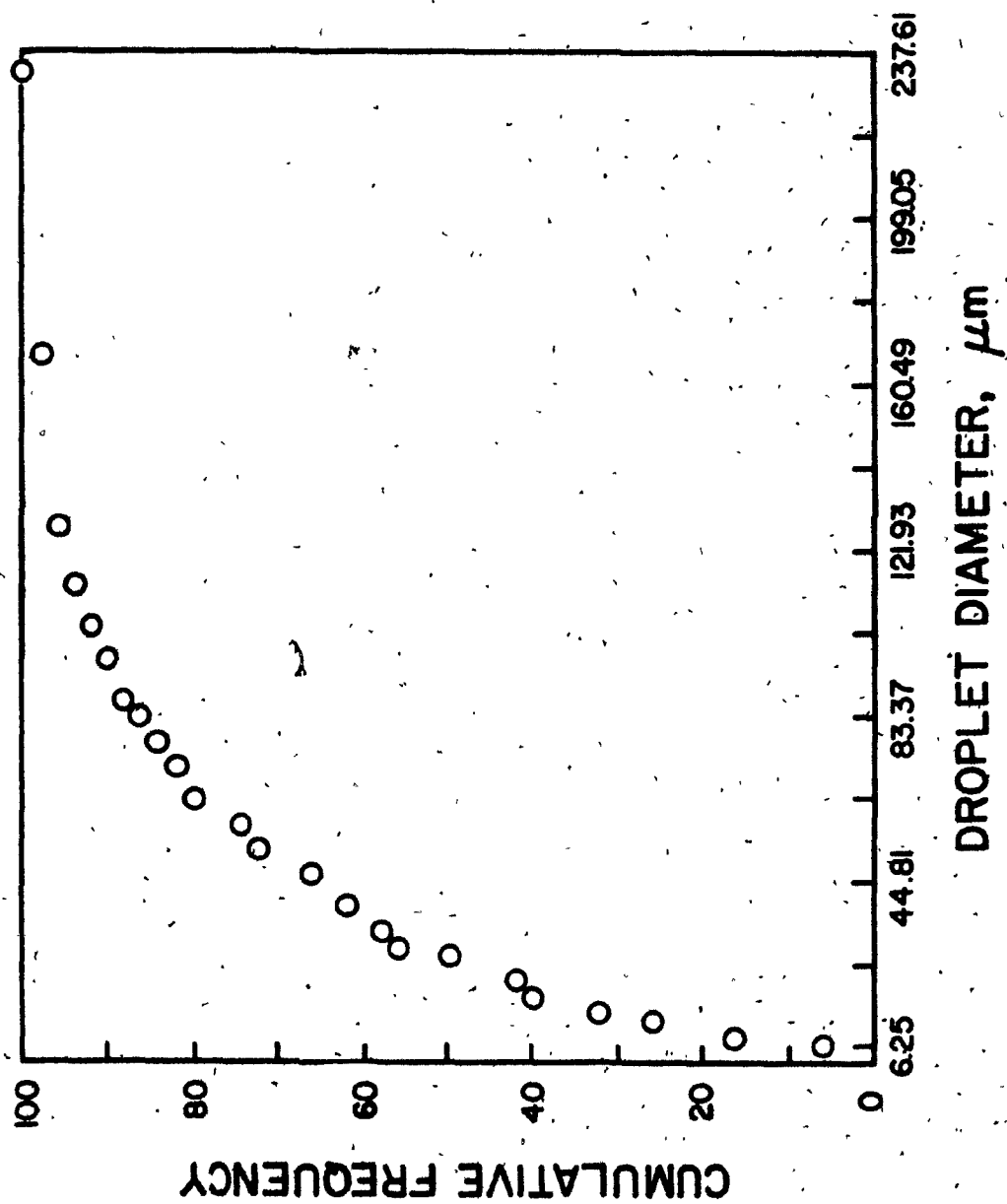




TABLE VIII  
RESULTS FOR PURE WATER, NOZZLE LNN2

| Run No. | $W_F$<br>kg/h | $p$<br>MPa | $P_{out}$<br>kW | Product Nature              | $T_o$<br>°C | $\Delta H$<br>kW | $q_L$<br>kW | $q_L$<br>% | $E$<br>kg/h | $\eta_1$<br>% | $\eta_2$<br>% |
|---------|---------------|------------|-----------------|-----------------------------|-------------|------------------|-------------|------------|-------------|---------------|---------------|
| H1      | 17            | 1.89       | 12.3            | WET<br>(dispersed droplets) | 200         | -                | -           | -          | -           | -             | -             |
| H2      | 16            | 1.55       | 12.3            | WET                         | 205         | -                | -           | -          | -           | -             | -             |
| H3      | 14.5          | 1.34       | 12.8            | DRY                         | 285         | 11.8             | 0.8         | 7          | 14.5        | 87            | 86            |
| H4      | 14            | 1.31       | 12.6            | DRY                         | 270         | 11.3             | 1.2         | 10.5       | 14          | 85            | 83            |
| H5      | 12.75         | 1.20       | 13              | DRY                         | 365         | 11.0             | 1.8         | 16         | 12.75       | 81            | 78.5          |
| I1      | 14            | 1.37       | 13              | DRY                         | 365         | 12.05            | 0.8         | 6.5        | 14          | 88            | 86            |
| I2      | 12            | 1.13       | 12.8            | DRY                         | 445         | 10.9             | 1.7         | 15         | 12          | 82            | 79            |
| I3      | 10.75         | 0.96       | 13              | DRY                         | 490         | 10.0             | 2.7         | 26         | 10.75       | 75            | 72            |

FIGURE 21

TEMPERATURE PROFILES FOR  
PURE WATER AND NOZZLE LNN2

RUNS H

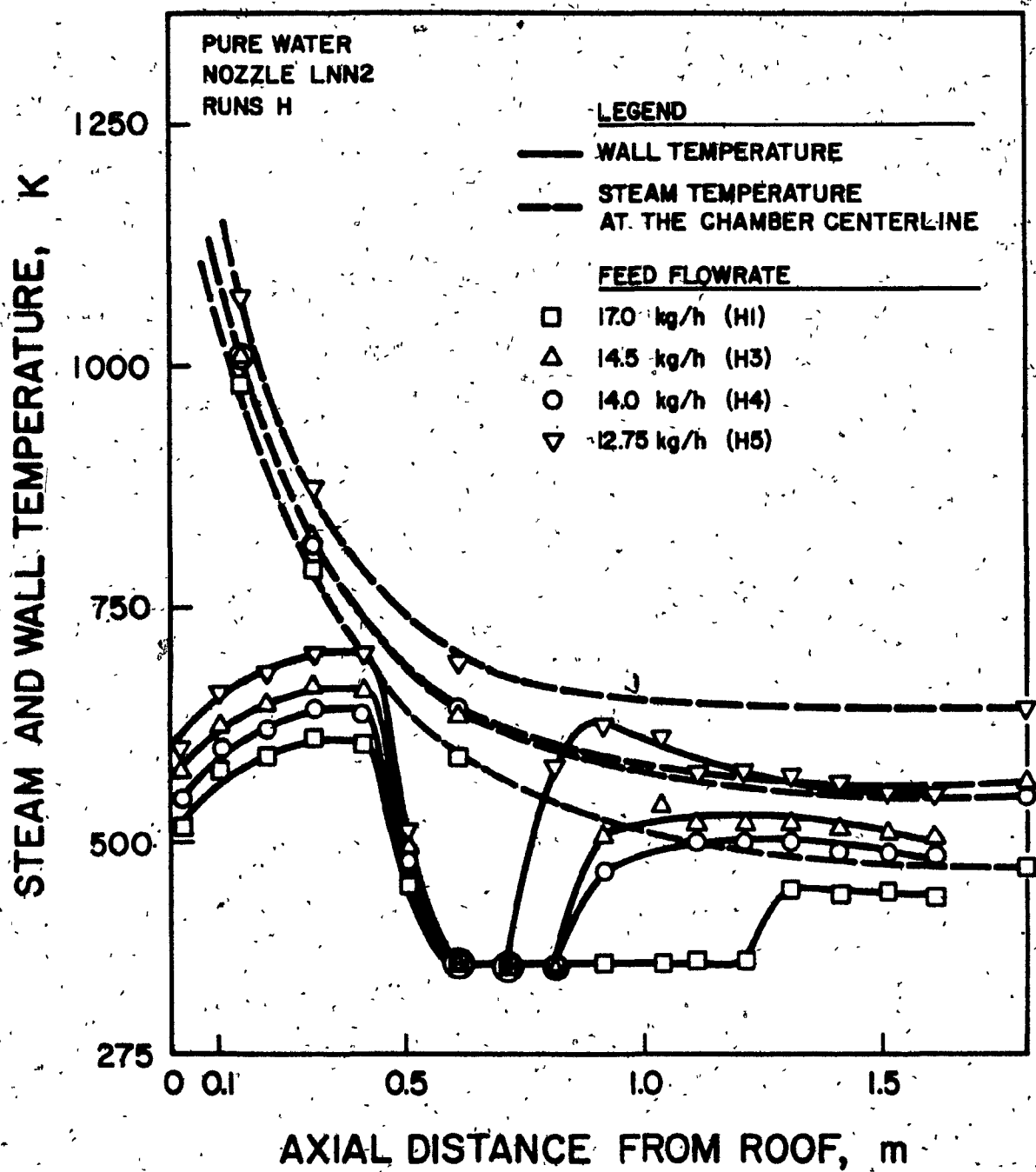
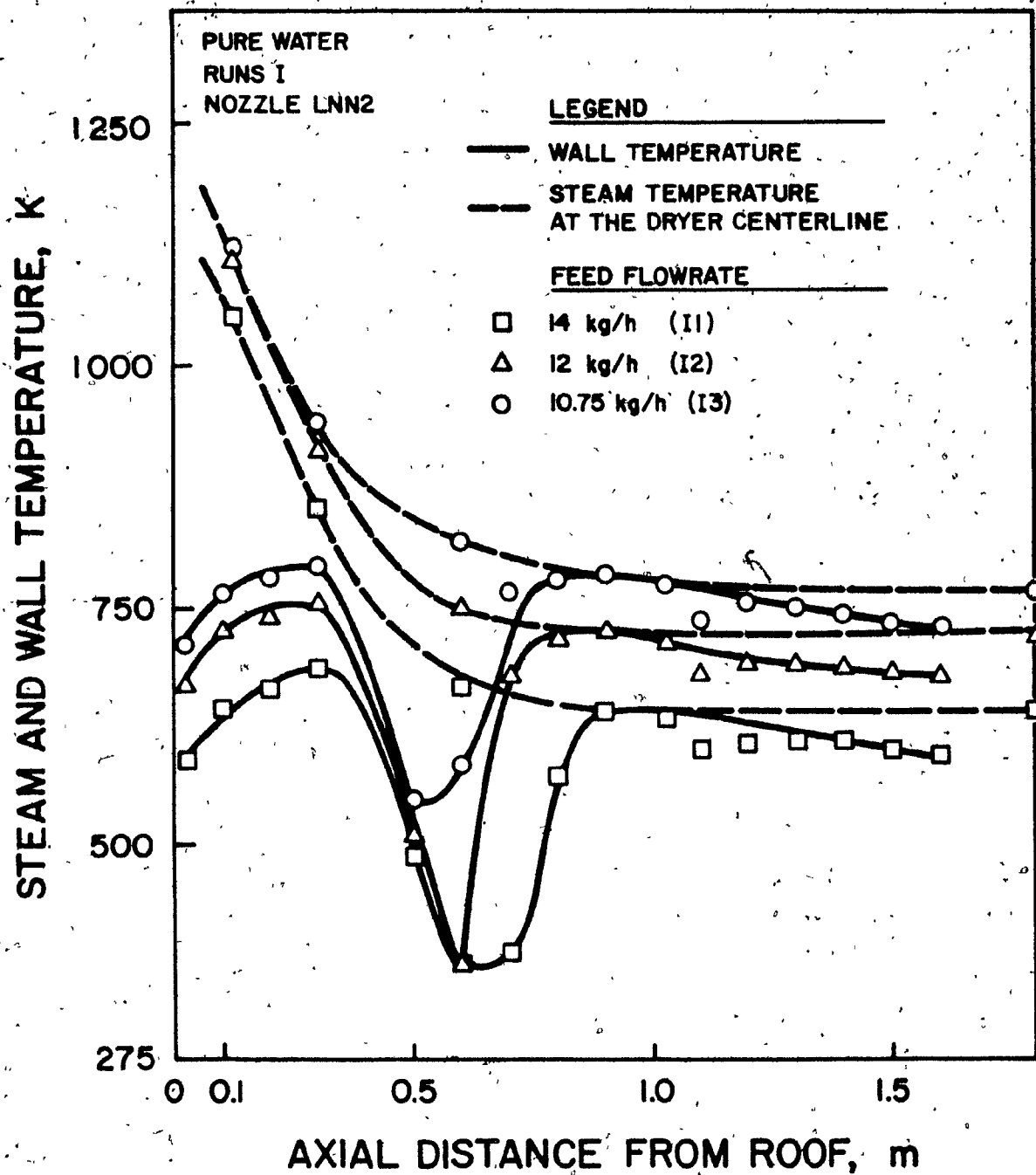




FIGURE 22

TEMPERATURE PROFILES FOR  
PURE WATER AND NOZZLE LNN2

RUNS I



comparable to those of H5 ( $12.75 \pm 0.6$  kg/h) and as the feed rate is decreased to 12 kg/h (I2) and then 10.75 kg/h (I3), the profiles are shifted upward by about  $75^{\circ}\text{C}$  and  $125^{\circ}\text{C}$ , respectively. For the smallest feed rate (I3), the wall temperature does not decrease to  $100^{\circ}\text{C}$  but to  $300^{\circ}\text{C}$  only at 60 cm from the roof, indicating that the largest droplets are dry by the time they reach the wall.

The initial drop size distribution has been obtained for 14.5 kg/h of water fed through the atomizer at a pressure of 195 psig (1.34 MPa gauge). The high pressure induced a fairly fine distribution (Figure 23) with a smooth cumulative frequency curve (Figure 24). The experimental and calculated values of  $D_{VS}$  and  $D_{MAX}$  are then not much different (Table IX). The statistical average diameter for Figure 23 was  $36.427\text{ }\mu\text{m}$ , the median  $25.633$  and the standard deviation  $28.000$ .

##### 5. Mode of Droplet Crystallization

The relevant photographs are presented in Appendix C. The dried product obtained from the drying of either 30%  $\text{NaNO}_3$  or 10%  $\text{NaNO}_3$  was basically formed of clusters, single cubical crystals and somewhat malformed cubes (Figures 5, 6, 8, 9 and 10 Appendix C). For 10%  $\text{NaNO}_3$ , the clusters appeared to be very irregular in shape consisting of many agglomerated single particles (Figures 5 and 6), whereas for 30%  $\text{NaNO}_3$ , they seemed to be more spherical and homogeneous, and much larger than the single crystals, so that they could result from the drying of the largest droplets with crust formation.

  
FIGURE 23

DROPLETS RELATIVE NUMBER DISTRIBUTION

PURE WATER AND NOZZLE LNN2

AT A PRESSURE OF 1.34 MPa GAUGE



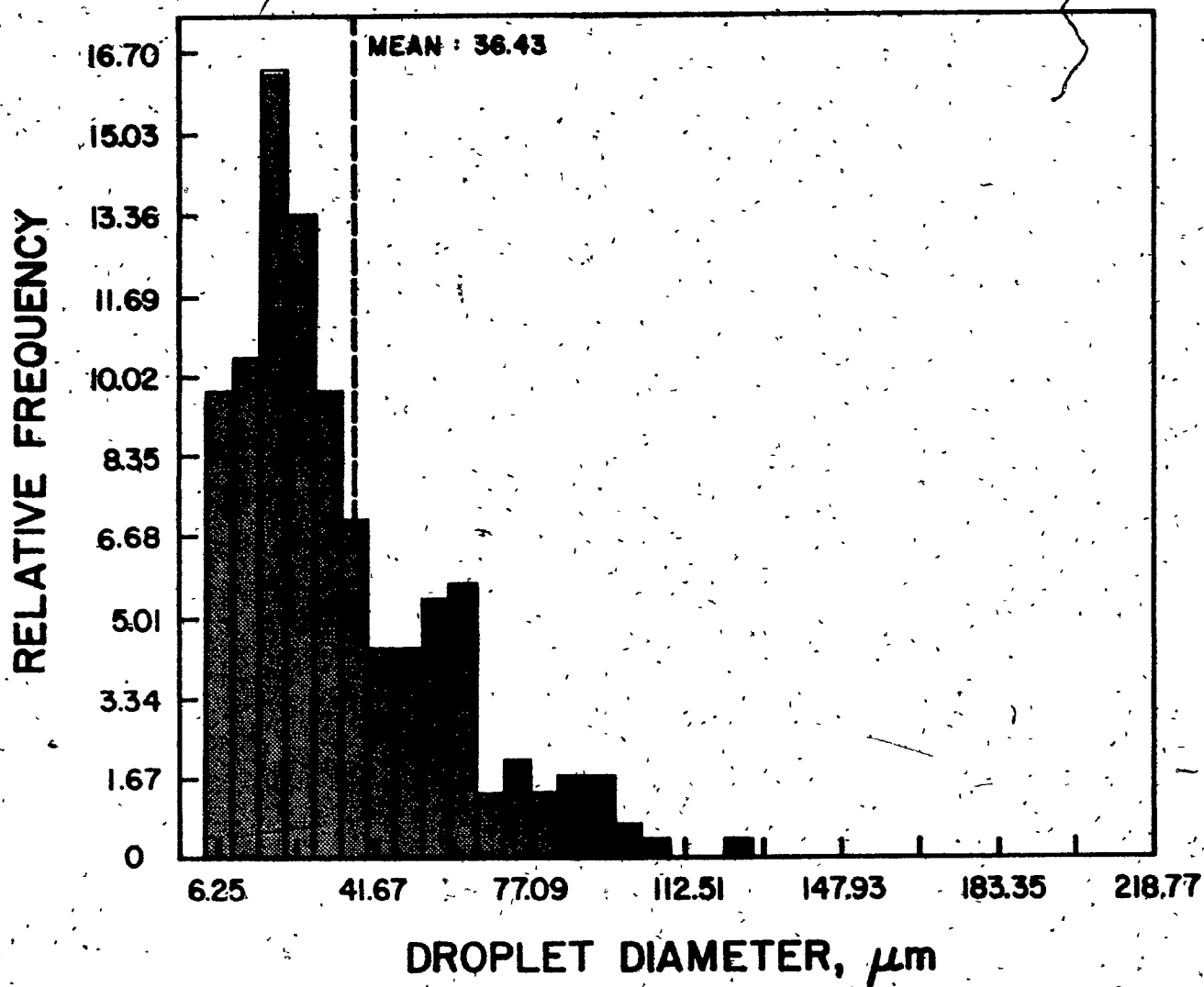




FIGURE 24DROPLETS CUMULATIVE NUMBER DISTRIBUTIONPURE WATER AND NOZZLE LNN2AT A PRESSURE OF 1.34 MPa GAUGE

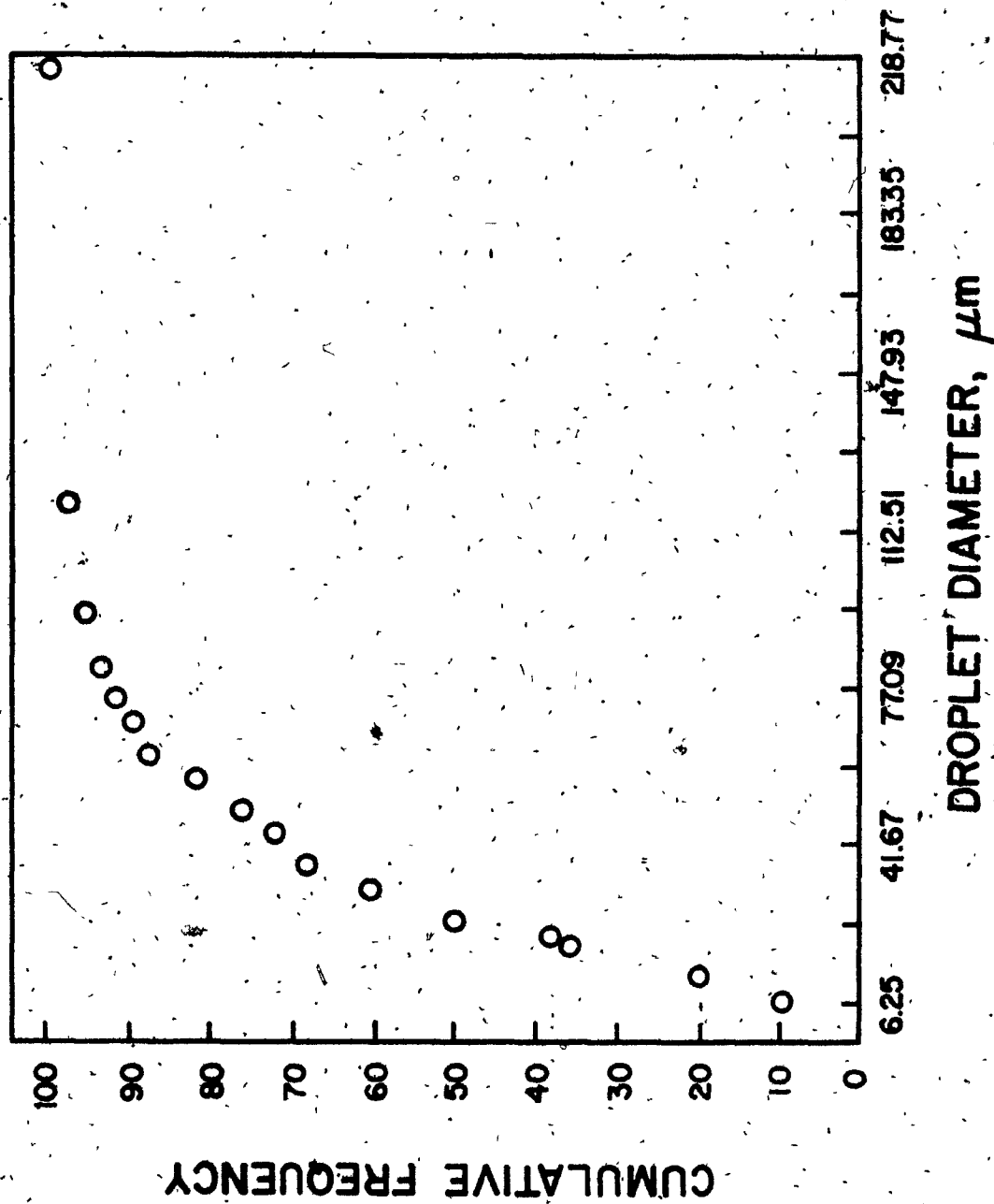


TABLE IX  
COMPARISON OF EXPERIMENTAL  
AND CALCULATED VALUES OF  $D_{VS}$  AND  $D_{MAX}$   
FOR PURE WATER, NOZZLE LNN2

|                                                                   | EXPERIMENTAL VALUE | CALCULATED VALUE | DIFFERENCE |
|-------------------------------------------------------------------|--------------------|------------------|------------|
| Sauter Mean Diameter<br>$D_{VS}$ ( $\mu\text{m}$ )                | 77                 | 77               | <1%        |
| Maximum Stable Droplet<br>Diameter<br>$D_{MAX}$ ( $\mu\text{m}$ ) | 219                | 180              | 18%        |

From these photographs, it appears that two major modes of drying could be distinguished for  $\text{NaNO}_3$  at high temperature, depending on whether small or large droplets at a high or low initial salt concentration are considered. Small droplets are very much influenced by the high temperature of the drying gas at the nozzle exit since they experience such a high evaporation rate that the migration of moisture within the drop cannot maintain surface wetness and a dried crust forms almost immediately in the region below the nozzle exit. This crust acts to retain moisture within the droplets and as that moisture heats up, it makes the droplets burst and form small particles of irregular shapes which may agglomerate (or not) in the bottom section of the chamber. In contrast, larger droplets do not seem to be nearly so affected by the initial high temperature since they have a large volume to evaporate, under free liquid conditions as long as they remain below saturation, and since they pass through the nozzle zone more rapidly. When they reach saturation (the time depending on their initial concentration) in a region of lower temperature, the evaporation rate is much lower and allows the formation and growth of one (or more) cubical crystal within each droplet.

The different appearance of the larger particles of dry product for 10% and 30%  $\text{NaNO}_3$  supports that conclusion: for 10%  $\text{NaNO}_3$ , nearly perfect cubical crystals can be seen besides the clusters whereas, for 30%, most of the larger particles present a spherical, puffy appearance probably due to crust formation.

The initial concentration of the solution is important for the larger drops since it determines the time and position within the dryer at which the droplet reaches saturation and also the drying steam temperature at that point, which influences the drying behavior.

For a solution of 30%  $\text{NaNO}_3$ , Baltas (1969) also observed internal crystallization but for inlet air temperatures from 100 to 150°C, whereas he felt that crust formation and internal crystallization occurred simultaneously in the larger droplets for an inlet air temperature of 198°C.

#### DISCUSSION OF THE RESULTS

1. The fairly good general agreement between the experimental and calculated Sauter Mean Diameters and maximum droplet diameters, which has been reported in this work, allows some confidence in the droplet size distribution. As 2 500 to 3 000 droplets were counted for each distribution, it can be inferred that, according to Azzopardi (1979), the deviation of the experimental Sauter mean diameters is less than 10% within the 95% confidence limits. It must be pointed out, however, that these distributions were obtained in saturated ambient room-temperature air instead of the superheated steam atmosphere existing in the spray dryer, in order to avoid serious experimental difficulties due to the intense evaporation which would have occurred just at the nozzle exit in the high temperature steam. That change of temperature and composition of

the surrounding gas medium has certainly some influence on the resulting drop size distribution; it is well-known that the atomization of a liquid is affected by the ambient gas density and that a decrease in density leads to the production of coarse drops. Thus, the initial drop sizes would be coarser while spraying into high temperature steam. However, this effect may be expected to be small for two reasons:

- ° the dependance of  $D_{VS}$  on the gas density has been shown to be rather weak. Dombrowski and Hooper (1962) found that  $D_{VS}$  varied inversely as the gas density to the  $1/4$  or  $1/6$  power
- ° the intense evaporation occurring immediately at the nozzle exit depresses the steam temperature and thereby increases its density.

All the other parameters which could have influenced the drop size distribution such as atomizing pressure, feed viscosity and density depending on its composition and temperature, were kept the same as they were in the actual drying runs, so that the experimental drop size distribution is fairly well representative of the initial spray before it begins to dry.

2. The steam temperature profiles at the centerline of the dryer and the walls temperature profiles show the same general trends for all the feed compositions studied: in the upper part of the dryer, up to 0.6 m from the roof, the steam temperature decreases

very steeply because of the high evaporation rate; it then decreases much more smoothly to reach an almost constant value in the last 80 to 100 cm of the chamber; that last section of the dryer at an almost constant low temperature corresponds to the free-entrainment zone where the final stage of drying proceeds at a very low rate. The walls of that section show also an almost constant temperature profile when steady state is reached. These flat profiles are both an evidence of the high turbulence level and of the resulting complete mixing of steam in that bottom section of the chamber.

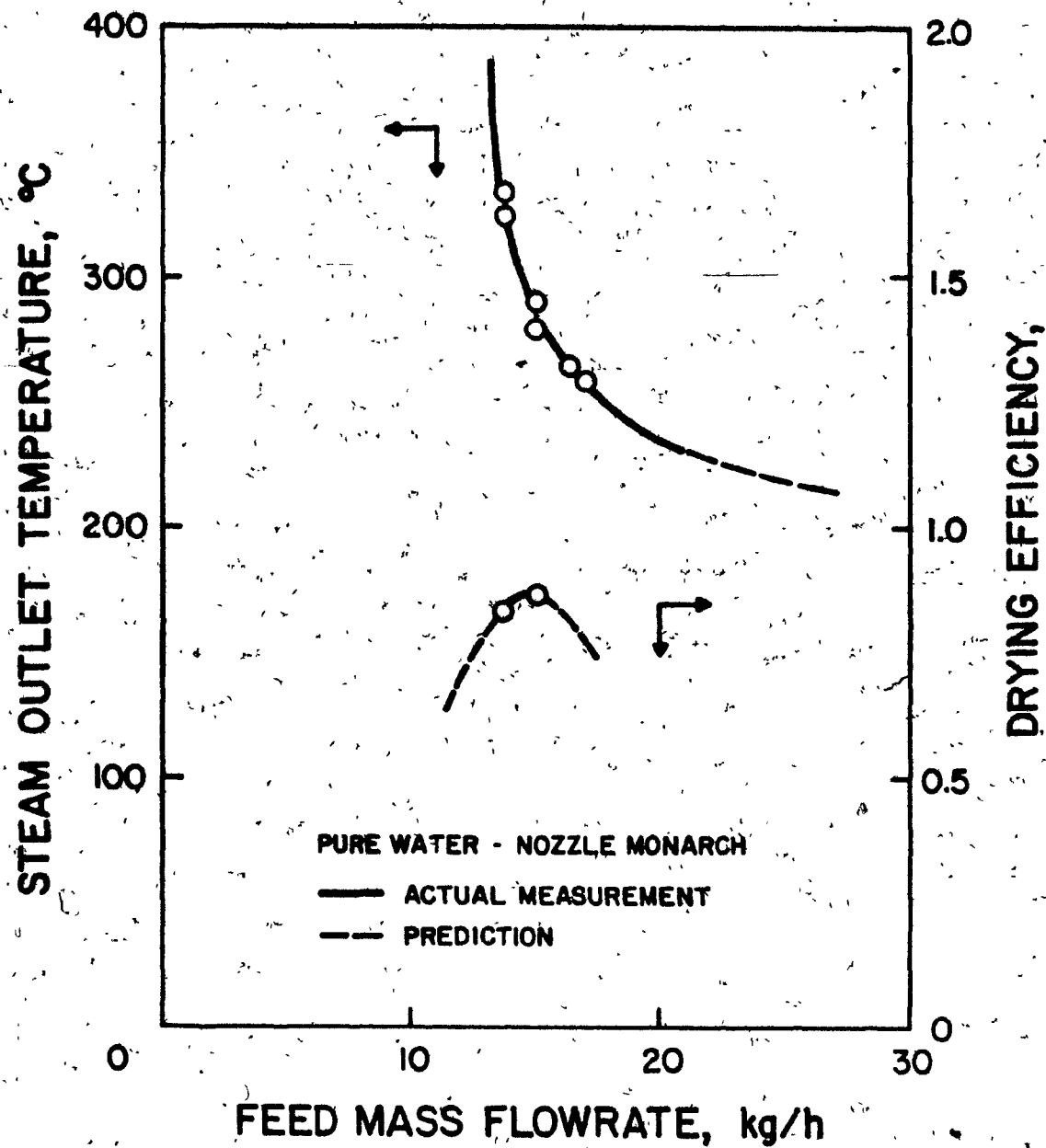
The wall temperature profile in the upper section of the chamber shows a parabolic trend which is characteristic of a plasma jet confined in a small diameter cylindrical reactor (Kubanek, 1966); however, because of the spray, the walls are cooled much more rapidly than they should normally be, and reach 100°C at some point where semi-wet droplets hit them; the temperature increases then to reach a constant value further beyond that section if the feed rate is sufficiently low, whereas it stays at 100°C for high feed rates. On the other hand, if the feed rate is such that the largest droplets at the periphery of the spray are nearly dry when they hit the walls, the wall temperature decreases directly to its constant value, usually in the range of 250 to 300°C.

3. A plot of the drying efficiency and outlet temperature against the feed mass flow rate (Figure 25) clearly shows that the optimum drying efficiency corresponds to an outlet temperature of 280 to 300°C; to the left of that optimum, the water loading is too

FIGURE 25

GRAPH OF DRYING EFFICIENCY AND  
STEAM OUTLET TEMPERATURE VERSUS  
FEED MASS FLOW RATE  
FOR PURE WATER AND MONARCH 4.0 NOZZLE





low to use all the energy of the gas stream for drying and energy is wasted even though the product is dry; furthermore, the product quality is rapidly lost due to overheating as the outlet temperature increases steeply. To the right of the optimum efficiency, the loading is too high to be completely dry and consequently, the steam temperature relative to the particles becomes much smaller, generating then a very low drying rate so that a considerable added length (and time) would be required for complete drying.

4. Another interesting conclusion can be deduced from a consideration of the experimental results obtained with pure water, but with different nozzles; a remarkable similarity of results was obtained (compare Figures 18 and 21), which is quite opposite to what was expected; it indicates first the small or even negligible influence of the impingement of water droplets on the walls, since the same evaporative capacity is obtained with both nozzles although the spray cone-angle generated by the LNN2 nozzle (Figure 21) is twice that of the Monarch 4.0 nozzle (Figure 18). This underlines that the drying process is indeed very rapid, almost instantaneous at the exit of the nozzle and that the most important variable in the operation is the power which is effectively fed to the system. However, that conclusion may be somewhat attenuated by the influence of the degree of atomization of the spray since the drop size distribution from the Monarch 4.0 nozzle is 40% coarser than the distribution from the LNN2 nozzle (compare Tables VII and IX). This should result in a lower heat transfer area and, therefore, a lower evaporative capacity.

5. The decrease of the maximum evaporative capacity of the chamber with the increase of the initial concentration of the solution to be dried is evident from Table X. It is interesting to note that an identical behavior was observed by Gauvin et al. (1975) in the case of conventional spray drying, using air as the drying medium. It is clearly opposite to the fact that the drop size distributions become increasingly finer with an increasing initial concentration of the solution and the same type of Monarch nozzle (the values of  $D_{VS}$  and  $D_{MAX}$  are getting smaller as the initial concentration of the solution increases). This behavior can only be attributed to lower evaporation and drying rates due to the formation of a rigid porous crust at the surface of the largest droplets, which contributes to heat transfer resistance; the formation of that crust occurs sooner as the initial concentration is higher (this has already been developed in the section entitled "Mode of Crystallization of the Droplets") and the influence of the high initial drying gas temperature is then much attenuated, resulting in a less efficient utilization of the energy passed through the dryer. The amount required to evaporate one kg of pure water is thus  $3\,150 \pm 30$  kJ/kg, whereas it is increased by respectively 5% and 13% as the feed is concentrated to 10% and 30%  $\text{NaNO}_3$ . However, the yield of dried product per unit time is much larger when the initial concentration of the feed is the highest in this operation, even if the corresponding evaporative capacity is slightly lower. According to Crowe (1980), the dryer should be designed for maximum specific heat consumption (i.e. the minimum

TABLE X

COMPARISON OF EXPERIMENTAL RESULTS

| Initial Concentration<br>to be Dried  | Maximum<br>Evaporative<br>Capacity<br>(kg/h) | Yield of Dried<br>Product Per<br>Unit Time<br>(kg/h) | Energy Requirement<br>to Evaporate One<br>kg of Water<br>(kJ/kg) | Energy Requirement<br>to Dry One kg<br>of Product<br>(kJ/kg) | Effective<br>Power of<br>Plasma<br>(kW) | Corresponding<br>Drying True<br>Efficiency $\eta_2$<br>(%) |
|---------------------------------------|----------------------------------------------|------------------------------------------------------|------------------------------------------------------------------|--------------------------------------------------------------|-----------------------------------------|------------------------------------------------------------|
| 30% $\text{NaNO}_3$<br>Monarch Nozzle | 12.75                                        | 5.45                                                 | 3 572                                                            | 8 356                                                        | 12.65                                   | 77                                                         |
| 10% $\text{NaNO}_3$<br>Monarch Nozzle | 14.0                                         | 1.55                                                 | 3 317                                                            | 29 961                                                       | 12.9                                    | 84                                                         |
| 0% $\text{NaNO}_3$<br>Monarch Nozzle  | 15.0                                         | -                                                    | 3 120                                                            | -                                                            | 13.0                                    | 87                                                         |
| 0% $\text{NaNO}_3$<br>LNN2 Nozzle     | 14.5                                         | -                                                    | 3 178                                                            | -                                                            | 12.8                                    | 86                                                         |

heat input requirement to produce a unit weight of dry product); it corresponds obviously here to the highest initial feed concentration (8 356 kJ/kg of dried product for 30%  $\text{NaNO}_3$ , compared to 29 961 kJ/kg for 10%  $\text{NaNO}_3$ ). That conclusion may not be true for all feed materials, since it clearly depends on the mode of crystallization and drying of the material and on its atomization properties at high concentrations.

### CONCLUSIONS

Although of an admittedly limited scope, this study has brought out some interesting aspects, which may be summarized as follows:

1. For sodium nitrate, as well as for materials which show the same drying behavior, optimum feed concentration is the highest concentration of the material which can be successfully atomized. The highest concentration may result in a slightly lowered evaporative capacity of the drying chamber, but it nevertheless gives the highest yield of dry product per unit time per unit of energy, when a constant power level is fed to the dryer; it corresponds thus to the minimum specific energy consumption, which industry is looking for (8 360 kJ/kg of dry sodium nitrate produced, see Table X).
2. An optimum range of feed rates corresponding to an optimum drying efficiency can be found for a given feed concentration and a given plasma torch power; it corresponds to a dryer outlet temperature

between 280 and 300°C for sodium nitrate. To the left of that optimum, the product quality deteriorates rapidly due to over-heating of the particles since the water loading is too low to use all the energy of the drying steam for the evaporation and drying process only, whereas, to its right, the loading is too high to be completely dried.

3. The major drawback of the operation is certainly, as it was predicted, that the outlet dryer temperature must be continuously and carefully monitored. At first sight, it might be argued that only non-heat-sensitive materials, with high melting and decomposition temperatures, can be dried in that chamber design, because of the intense turbulence and recirculation phenomena which occur in it. However, it is felt that heat-sensitive products might also be dried by that process, provided a very short residence time (nozzle zone range only) is used, with a fairly wide chamber, specially designed to minimize turbulence and recirculation.

4. Particularly important is also the necessity of relatively uniform droplets generated by atomization, since oversized droplets need much time (and length) to achieve complete drying in the bottom of the chamber where the temperature is almost constant, equal to the relatively low outlet temperature, whereas undersized droplets rapidly dry and cause fine dust which may become overheated. Moreover, a spray of narrow drop size distribution will evaporate completely more rapidly than one of the same mean diameter but of a wider drop size distribution, even though the initial evaporation rate will be lower.

Finally, a brief comparison with conventional hot-air spray drying may be drawn:

The total energy supplied to the dryers is very well utilized in the new drying process, resulting in an efficiency of about 80% for the spray drying operation itself, if the energy contained in the outlet steam can be successfully recovered for subsequent plant use. It can be then favorably compared to conventional hot-air spray dryers, for which the efficiency seldom exceeds 60%. This aspect appears to be very promising for a further development of the process.

Another important variable for such a comparison is the maximum evaporative capacity of the drying chamber expressed in  $\text{kg}/(\text{h} \times \text{m}^3)$  of chamber volume, since this quantity relates directly to the capital cost of a spray drying operation. For cross- and co-current operation in conventional hot-air spray dryers, with drying temperatures between 150 and 200°C, Keey (1972) reports that capacities lie in the range from 1 to 5  $\text{kg}/(\text{h} \times \text{m}^3)$ , for both pressure nozzle and rotary disc atomization. For 30%  $\text{NaNO}_3$ , in the pilot-plant installation used in this study, the evaporative capacity was about 24.5  $\text{kg}/(\text{h} \times \text{m}^3)$  of chamber volume, which is much larger than the conventional capacity.

The experimental findings reported in this study appear to confirm the predictions of a recent techno-economic assessment of this novel approach to spray drying (Gauvin, 1981). The high

evaporative capacity, the absence of compressors and effluent treatment equipment, the heat recovery aspects and greatly enhanced thermal efficiency, all combine to allow major savings in both capital and operating costs. It must be recognized, however, that none of the existing commercial plasma devices have been used, on a sustained basis, with superheated water vapour. Owing to the aggressive nature of dissociated steam, it is anticipated that special metals, such as zirconium, will have to be used to withstand the corrosive action of the superheated water vapour. It should be pointed out, however, that the commercial applications of this novel technique will not have to await the development of torches of large power. Indeed, it is envisaged that a plurality of smaller torches (say 200 to 500 kW) strategically located down the length of the spray dryer would be far better as far as the maintenance of the desired temperature profile is concerned, than the use of a single device of high power on top of the dryer.



### NOMENCLATURE

|                            |                                                                                       |
|----------------------------|---------------------------------------------------------------------------------------|
| $C_F$                      | - feed solid concentration (%)                                                        |
| $d_i$                      | - droplet diameter ( $\mu\text{m}$ )                                                  |
| $D_{\text{MAX}}$           | - largest stable droplet diameter ( $\mu\text{m}$ )                                   |
| $D_{\text{VS}}$            | - Sauter mean diameter ( $\mu\text{m}$ )                                              |
| $E$                        | - evaporative capacity (kg/h)                                                         |
| $H_F$                      | - specific enthalpy of feed (J/kg)                                                    |
| $H_{\text{Gin}}$           | - specific enthalpy of the low-temperature steam entered into the plasma torch (J/kg) |
| $H_O$                      | - specific enthalpy of steam at outlet temperature (J/kg)                             |
| $H_{\text{Po}}$            | - specific enthalpy of dry product at outlet temperature or melting point (J/kg)      |
| $H_{\text{To}}$            | - specific enthalpy of outlet mixture (J/kg)                                          |
| $H_{\text{(water, 20°C)}}$ | - specific enthalpy of liquid water at 20°C (J/kg)                                    |

- $\Delta H$  - heat transferred to feed, as defined by Equation (7), (kW)
- $I$  - arc current intensity (A)
- $n_i$  - number of droplets of diameter  $d_i$
- $P_{\text{atomization}}$  - power fed for atomizing the feed solution (kW)
- $P_{\text{out}}$  - effective power fed to the plasma gas (kW)
- $P$  - atomization pressure, (MPa), gauge
- $q_L$  - overall heat losses through the dryer walls (kW)
- $T_F$  - temperature of feed (K)
- $T_{\text{Gin}}$  - temperature of low-temperature steam entered into the plasma torch (K)
- $T_o$  - outlet temperature (K)
- $U$  - arc voltage (volts)
- $W_F$  - mass flow rate of feed (kg/h)
- $W_G$  - mass flow rate of "plasma" steam (kg/h)
- $W_o$  - mass flow rate of steam at the outlet of the dryer (kg/h)
- $W_p$  - mass flow rate of dried product at the outlet of the dryer (kg/h)

$W_{T0}$ 

- total mass flow rate of the mixture at the outlet of the dryer (kg/h)

 $\eta_1$ 

- drying efficiency of the actual operation (%)

 $\eta_2$ 

- drying efficiency of the "true" operation with a pure steam plasma of same effective power under otherwise the same conditions (%)

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EXPERIMENTAL SECTION

PART II

SIMULATION OF THE EXPERIMENTAL SYSTEM

## EXPERIMENTAL SECTION

### PART II

#### INTRODUCTION

Simulation of an experimental work is generally very useful to understand the basic mechanisms of the process which is being studied.

During a spray drying test operation in a given chamber-- design and under fixed operating conditions, the important variables studied are the time and the distance required to complete the drying of the spray of droplets; these variables depend on the rate of heat and mass transfer between the droplets and the drying gas as well as on the residence time of the droplets in the drying chamber; these, in turn, are both dependent on the drying gas and droplets flow patterns and on the contacting between the two phases. Spray drying involves, then, a great number of fundamental chemical engineering operations which are completely interdependent, such as heat, mass and momentum transfer between a gaseous continuous phase and a liquid dispersed phase, involving three-dimensional considerations since vortex motion is generally used. The system is



further complicated by the effects of the presence of an atomizing device on the flow pattern, by secondary circulation and turbulence phenomena, and, in certain cases, by crystallization considerations during product formation. These complexities make the modelling of such a system more difficult but, on the other hand, that modelling facilitates the interpretation of the experimental evidence and the understanding of the phenomena involved.

Many models have been proposed in the literature to describe spray drying operations. They usually apply to a single specific chamber design with a known drying gas flow configuration, and are usually based on a large number of empirical correlations and assumptions.

One of these models has been developed by Gauvin and Katta (1976) following a series of preliminary studies (Katta and Gauvin, 1975; Gauvin, Katta and Knelman, 1975) to design a cocurrent spray drying chamber with single or multiple tangential air inlet. This model can be adapted to other chamber configurations, but its limitation is that the flow pattern must be mathematically defined. This model has already been modified to simulate spray drying operation with a superheated steam plasma in a cylindrical chamber, in a preliminary theoretical study by Gauvin (1981). The author feels that it would be worthwhile to test this program in the present experimental situation and compare predicted and experimental results: it might lead to some more realistic assumptions within the

program and, on the other hand, to a better understanding of the actual physical process:

In the treatment that follows, a general description of the model and of its basic assumptions is first given. The heat and mass balances are then presented, together with certain modifications that were introduced to obtain a better agreement between the experimental and the simulated operating conditions. Finally, the data entered into the program as specifications required by the simulated spray drying operation are listed and the results discussed.

#### DESCRIPTION OF THE MODEL

##### General Presentation

The program described below is based on a generalized stepwise procedure which predicts the dimensions of a spray drying chamber according to the required rate of production of the powder, the product specifications and the operating conditions which have been selected. On the other hand, it can also be used to predict the maximum evaporative capacity and thermal efficiency of a spray drying chamber for a given set of operating conditions, according to a selected criterion of drying. As mentioned above, it was first developed by Gauvin and Katta (1976) to design conventional hot air spray drying chambers with tangential air inlets; it was then modified by Gauvin and Costin (1980) to take into account the

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use of superheated steam as the drying medium, and finally by Gauvin (1981) to consider a plasma of water vapour as the heat source. The drying chamber is divided into three zones: a mixing zone at the top of the chamber above the atomizer to account for the introduction of the plasma, a nozzle zone just below the atomizer where the droplets issuing at a high velocity from the nozzle decelerate rapidly to approach their terminal velocity, and a free-entrainment zone where the droplets have almost reached their terminal velocity and where the spray jet "breaks".

- ° The mixing zone is regarded as a "black box" where intense back-mixing and turbulence is generated by the expansion of the plasma jet and its pumping effect. As the calculations begin at some distance below the atomizing nozzle and as the plasma gas decelerates very rapidly at the exit of the torch, the complex steam flow pattern is not considered at all; an overall heat balance only is performed which gives the amount of back-mixed steam required to produce the steep axial temperature gradient recorded in that zone.

- ° In the nozzle zone, the dryer cross-sectional area is subdivided into a spray area in the center and an annular area surrounding the spray. A large amount of steam is entrained within the spray, as a

result of the high momentum transfer between the droplets and the surrounding steam. The temperature of that "drying steam" within the spray is much lower than that of the surrounding steam in the annular region since it is mixed with a large amount of evaporating steam which is generated at the boiling point of the solution in the droplets.

- ° The free-entrainment zone is characterized by complete mixing of the steam within the spray with the surrounding steam and by a resulting smaller drying rate because of the lower temperature of the mixed steam.

The spray drying chamber size is primarily determined by the time needed to dry the largest droplet up to the required residual moisture content and also by the trajectories of the droplets.

Published experimental data and correlations are used to predict the flow patterns of the drying gas and of the atomized droplets both in the nozzle zone and in the free-entrainment zone; the exact calculation of the droplets trajectories requires simultaneously solving:

- ° the equations describing the motion of the droplets
- ° the instantaneous rate of evaporation and drying of the droplets based on the widely-quoted Ranz and Marshall (1952) correlation

- ° the equations describing the motion of the drying gas
- ° the instantaneous properties of the drying gas.

To be able to solve simultaneously all these equations, the initial drop size distribution is divided into a small number of classes represented by their median diameters, and a variable time increment depending on the median diameter is chosen for each class so that all classes of droplets advance to the same plane perpendicular to the axis of the chamber. The distance travelled by all classes of droplets is fixed by the velocity of the smallest droplet which has not yet been dried.

The selection of a suitable criterion of drying is particularly important since it determines the point up to which the calculations proceed and thus the dimensions of the dryer. Experimental results should be obtained under the same criterion of drying in order to be comparable to the simulation results; this criterion will be generally determined from practical experience or from published industrial product specifications. It can be:

- ° a given residual moisture content for the largest drop before it reaches the chamber wall
- ° a given residual moisture content for the largest drop at the exit of the chamber

- ° a fixed residual moisture content for the dried product
- ° a maximum thermal efficiency for the whole operation.

#### Basic Assumption in Support of the Validity of the Simulation

As previously explained, because of external difficulties, the plasma used in the experimental work was a nitrogen plasma into which superheated steam was injected; the resulting drying gas at the exit of the plasma torch was then a highly superheated mixture of nitrogen and steam (about 50% by mass).

The drying conditions in pure steam and in a mixture of nitrogen and steam are obviously different: the droplet surface temperature and thus the thermal driving force is lower in the latter case, the heat transfer coefficients are not the same and the diffusional resistance to mass transfer can not be neglected in the mixture; at these high temperatures, it is likely that the resulting evaporation rate should be lower in the mixture of nitrogen and steam than in pure steam.

However, if we look at the gas emerging from the nozzle, its composition is only 39% nitrogen by volume. This is very rapidly decreased by back-mixing due to the entrainment caused by the pumping action of the jet. For the gas at the exit of the reactor, the

nitrogen concentration is much lower due to the large amount of evaporation which has taken place within the chamber: nitrogen at the exit is only 6% by volume of the exit gas. Thus, owing to the small volume fraction of nitrogen in the drying gas and to its much lower heat capacity, it is assumed that the experimental drying proceeds as if it were carried out in a pure superheated steam atmosphere. Under that assumption, the simulation which considers a pure superheated steam plasma as the heat source can be used and its results can be successfully compared to the experimental conditions; it will be based on a mass flow rate of plasma steam "equivalent" corresponding to the actual temperature and enthalpy of the mixture of steam and nitrogen at the nozzle exit. According to a heat balance performed at the exit of the torch, this equivalent mass flow rate of plasma steam is only 15% higher than the actual mass flow rate of steam injected in the experimental nitrogen plasma flame. It proves indeed that nitrogen carries a very small fraction of the total energy in the drying process and that it can be neglected with confidence.

#### Other Important Assumptions

1. The liquid feed is introduced in the experimental system at a temperature  $T_F$  which is different from the boiling point of the saturated solution. In practice, the liquid feed is generally preheated by heat exchange with a waste stream to improve the thermal efficiency. In addition, in most atomizing devices, an increase in

feed temperature results in a lowering of  $D_{VS}$  in the spray.

In the present operation, the feed was introduced at  $T_F = 10^\circ\text{C}$  and the amount of energy which is needed to heat the solution from  $T_F$  up to the boiling point is considered in the heat balances. Two variables  $H_{11}(C_1)$  and  $H_{12}(C_1)$  are introduced to account for the enthalpy of a solution of  $\text{NaNO}_3$  at a temperature  $C_1$  at respectively the entrance temperature  $T_F$  and the boiling point  $T_W$ .

2. The heat transfer equation is the widely quoted Ranz and Marshall correlation, corrected for high temperature by Spalding parameter:

$$Nu_{\text{actual}} = Nu_{\text{apparent}} \left[ \ln(1 + B)/B \right] \quad (1)$$

Other factors influencing  $Nu$  are particle rotation and free-stream turbulence (Clift and Gauvin, 1970). No data are available for particle-rotation effects on  $Nu$  for  $Re < 1500$ , but it is unlikely that the gas flow field can induce a particle rotation sufficiently large to effect a significant change on  $Nu$ . Extrapolating the data available in the literature for turbulence effects on heat transfer, one can predict turbulence should not cause an increase of more than 5% in  $Nu$  in spray drying applications.

3. The drying droplets can be considered to remain perfectly spherical and rigid during the whole drying process since the Reynolds Number never exceeds 50 for the largest droplets;



Calderbank and Korchinski (1956) and Hu and Kintner (1955) have indeed respectively shown that the drops exhibit very little deformation as long as their Reynolds Number is less than 200 and 300. However, according to Beard and Pruppacher (1969), the drag on drops becomes increasingly larger than the drag on solid spheres for Reynolds Number exceeding 200, indicating their progressive departure from a spherical shape.

4. For a solution, the drying rate is affected by the change in solid concentration within the droplets as time proceeds, the resulting increase of the boiling temperature at their surface and the variation of the latent heat of vaporization with that temperature. However, owing to their small size, the concentration and temperature are assumed to be uniform within the atomized droplets.

As a matter of fact, the assumption of uniform temperature and concentration within the droplet is valid as long as the Biot Number is less than 0.1 (Crowe, 1980). This number characterizes the relative importance of the internal heat (or mass) transfer over the external transfer. Considering the temperature profile inside the atomized droplets actually generated at these high temperatures (about 750°C), the Biot Number for heat transfer is:

$$Bi = Nu \, k / 6 \, k_d \quad (2)$$

and it can be approximated by:

$$Bi \approx Nu / 60 \quad (3)$$

The Reynolds Number for the largest droplets never exceeds 50 in the present analysis, and the maximum value of the Nusselt Number according to the Ranz and Marshall correlation with the Prandtl Number close to 1 is thus 6.25; it gives a maximum value for the Biot Number of

$$Bi_{MAX} \approx 0.104 \quad (4)$$

As this approximated value of the Biot Number is fairly close to 0.1 for the largest droplets, the effect of internal heat transfer within all the droplets can be neglected with some confidence and the assumption of uniform temperature and uniform concentration as well is then justified.

If the internal heat transfer could not be neglected compared to the external heat transfer - which is not really expected in spray drying, owing to the very small droplets formed in the atomization process - a very interesting model to describe the temperature profile within a vaporizing droplet has been derived by Yefremova and Smirnov (1977).

Because of the extremely high rate of evaporation, the droplet surface is assumed to be saturated (67% by weight, for  $\text{NaNO}_3$  solution) and at the boiling temperature of the saturated solution (392 K) corresponding to the chamber pressure throughout the entire period of drying.

5. The radiation flux from the surrounding high temperature steam to the liquid droplets is assumed to be negligible compared to the convective heat flux. This is indeed true for the smaller droplets with a radius below 100  $\mu\text{m}$ ; for the larger droplets at these high temperatures, it will give a maximum discrepancy of about 5%, based on the conclusions of Golovin and Pesochin (1977) which is quite acceptable for the purpose of this simulation.

6. Several properties of steam are needed in the calculations.

Thermodynamic properties are easily obtained from Ihara's tables (1977) within the range 1 000 K - 6 000 K, the reference state being 1 000 K.

As a first approximation, values of thermal conductivity and viscosity are obtained respectively from Hilsenrath et al. (1955) and from Potter (1949);

$$k = k_0 + 4.593 \times 10^{-3} [10^{0.93 \times 10^3} / T^4 - 1] \quad (5)$$

$$\text{where } k_0 = 6.4753 \times 10^{-3} \sqrt{T} (1 + 1737.3 \times 10^{(-12/T)/T}) \quad (6)$$

$$\text{and } \mu = 3.366 \times 10^{-8} T + 9.57 \times 10^{-7} \quad (7)$$

Finally, the density of steam is easily obtained by means of the ideal gas law, when the degree of dissociation is known. All the other assumptions and correlations (drag coefficient correlation, initial velocity and position of each class of droplets, droplets flow patterns, negligible radial temperature gradients, etc.) are

the same as those used by Gauvin (1981) in his preliminary study and they are reported in his paper and in the one by Gauvin and Katta (1976).

As expected, several major assumptions had to be reconsidered after some runs to get a steam temperature profile within the reactor more realistic and closer to the experimental one; they will be discussed in a further section.

#### HEAT AND MASS BALANCES

The symbols used in the following treatment are those depicted on Figure 1.

#### Overall Heat Balance

The amount  $W_G$  of steam plasma required for the drying process is given by the overall heat balance on the chamber:

$$W_G H_G + W_F H_F = (W_O H_O + W_P H_{P_O}) + q_L \quad (8)$$

$q_L$  is the total heat lost through the dryer walls, and is expressed as a given percentage of the total heat gained by the particles and their moisture. It was determined experimentally.

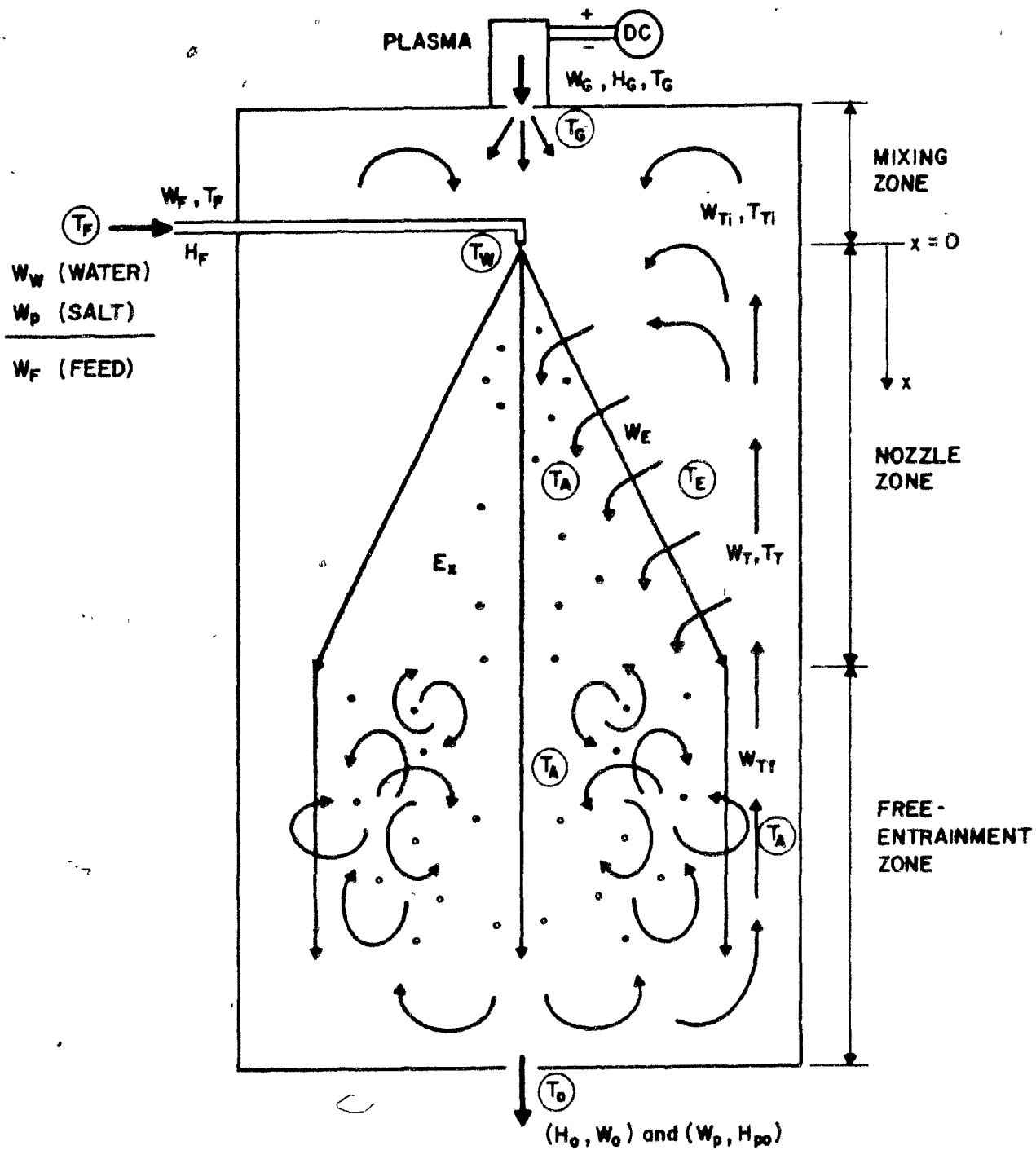
#### In the Mixing Zone

The initial amount of back-mixed steam  $W_{T_1}$  is computed from an overall heat balance on that zone, assuming no heat losses:

$$W_G H_G + W_{T_1} H_{T_1} = (W_G + W_{T_1}) H_M \quad (9)$$

FIGURE 1

SCHEMATIC DIAGRAM OF THE  
SPRAY DRYING CHAMBER



where  $H_M$  is the enthalpy of the mixed steam at the nozzle level, computed from the experimentally-known steam temperature.

$(W_{T_1} H_{T_1})$  is the total enthalpy of the recirculating steam as it enters the mixing zone above the atomizing nozzle to dilute the plasma jet.

#### In the Nozzle Zone

Let us consider a control volume of the chamber between the nozzle and a distance  $x$  from the nozzle. The jet is formed of dispersed liquid droplets conveyed by the "drying steam" at a temperature  $T_A$ , which is uniform in a radial direction and is completely impervious to the "surrounding steam" at  $T_E$ , except for entrainment.

The temperatures  $T_A$  and  $T_E$  are calculated from the following heat balances.

#### A. For the Drying Steam at $T_A$

The energy which is transferred by the drying steam in the jet between  $x = 0$  and  $x$  to the droplets is only used to evaporate a given fraction  $E_x$  of the total evaporation in the dryer, since the whole feed rate is assumed to be instantaneously heated up to its boiling point at  $T_W$  just at the exit of the atomizing nozzle. The latter temperature is taken as the datum: .

$$W_E H_E = (W_E + E_x) H_{T_A} + E_x \lambda \quad (10)$$

Some energy is given to heat the fraction  $W_{P_x}$  of dried product generated by the evaporation, up to the temperature  $T_A$  at point  $x$ .

$W_E$  is the entrainment rate of the "surrounding steam" within the spray and it is correlated according to Benatt and Eisenklam Equation (1969).

#### B. For the Surrounding Steam at $T_E$

The energy lost by the surrounding steam between  $x = 0$  and  $x$  is the heat entrained within the spray ( $W_E H_E$ ); however, a small fraction of it ( $q_{Lx}$ ) is indeed really lost through the dryer walls:

$$W_G H_G - W_E H_E + W_T H_T = (W_G + W_T - W_E) H_E + q_{Lx} \quad (11)$$

The energy required to heat the whole feed up to its boiling temperature  $T_W$  is neglected since  $T_W$  is the temperature datum. It is assumed to be instantaneously provided by the plasma jet at the exit of the atomizing nozzle, or from an external source (to lessen the load on the plasma torch).

$(W_T H_T)$  is the total energy brought by the recirculating steam which is continuously heating up as it goes up in the chamber.

#### In the Free-Entrainment Zone

The steam within the spray and the surrounding steam are mixed since the jet is now broken, its temperature is  $T_A$ , and the energy which has been lost by the mixed steam at the abscissa  $x$



has either been gained by the drying droplets or lost through the dryer walls. With  $T_W$  as the datum, and ignoring the enthalpy of the solid product, the heat balance is:

$$(W_G + E_x) H_{T_A} = W_G H_G - E_x \lambda - q_{Lx} \quad (12)$$

### MODIFICATIONS

#### 1. Rate of Entrainment of Steam Within the Jet

The semi-empirical equation of Benatt and Eisenklam (1969) which determines the amount of gas entrained within a liquid jet is strictly applicable to non-condensable gases, at ambient conditions. However, it is felt that it can describe the entrainment of highly superheated steam within the spray: the droplets reach the boiling point almost instantaneously because of the high temperature of the surrounding steam, and the condensation of the entrained steam on the droplets should thus be negligible.

The amount of gas entrained within the spray is given by:

$$W_E = E_p (x - x') \sqrt{W_F V_n \rho_G} \quad (13)$$

where  $E_p$ , the entrainment parameter is equal to

$$E_p = 0.96 \tan \theta / \sqrt{\cos \theta} \quad (14)$$

up to a certain distance  $x'$  which is dependant on the length of the liquid sheet at the exit of the nozzle. Beyond that distance, the

entrainment within the spray is comparable to that of a gas jet and  $E_p$  is a constant, equal to 0.282. The values of  $x'$ ,  $x''$  and  $E$  are given in Table I for the 4 different feed compositions studied.

In his model, Gauvin (1981) had arbitrarily reduced by a factor of four the amount of entrained steam to account for the large volume changes generated by the evaporation process and for the small diameter of the chamber. Using his model, a comparison has been made of the calculated and the experimental temperature profiles of the steam along the centerline of the chamber for the four experimental runs; the calculated temperature  $T_A$  of the "drying steam" appeared much too low, whereas the temperature  $T_E$  of the "surrounding steam" in the annulus was too high, obviously resulting from a too small rate of entrainment of the highly superheated surrounding steam within the spray. When the rate of entrainment of steam within the spray was divided by 2, the same trend was observed. However, when the total rate of entrainment  $W_E$  was considered, the agreement was considerably better and the profile for  $T_A$  was much closer to the experimental curve. These profiles are plotted on Figures 2 and 3 for 10%  $\text{NaNO}_3$  and 30%  $\text{NaNO}_3$  solutions respectively.

Higher values of the entrainment rate such as twice the value correlated by Benatt and Eisenklam (1969) did not lead to better results since, in these cases, the temperature of the drying steam within the jet worked out to be higher than that of the surrounding steam. Moreover, important fluctuations of these temperatures were introduced in the nozzle zone, because of the

**TABLE I**  
**VALUES USED IN THE CORRELATION OF THE**  
**ENTRAINMENT RATE WITHIN THE SPRAY**

|                              | $x'$<br>(m)          | $x''$<br>Maximum distance (m)<br>beyond which $\dot{m}$ does<br>not vary linearly with $x$ | E<br>Value of entrainment parameter<br>used in Equation (13) |
|------------------------------|----------------------|--------------------------------------------------------------------------------------------|--------------------------------------------------------------|
| Run A<br>30% $\text{NaNO}_3$ | $1.31 \cdot 10^{-2}$ | 0.197 m                                                                                    | E = 0.262 up to $x'' = 0.200$ m<br>E = 0.282 for $x > x''$   |
| Run B<br>10% $\text{NaNQ}_3$ | $1.48 \cdot 10^{-2}$ | 0.214 m                                                                                    | E = 0.262 up to $x'' = 0.210$ m<br>E = 0.282 for $x > x''$   |
| Run C<br>Pure Water-Monarch  | $1.45 \cdot 10^{-2}$ | 0.210 m                                                                                    | E = 0.262 up to $x'' = 0.210$ m<br>E = 0.282 for $x > x''$   |
| Run D<br>Pure Water-LNN2     | $1.20 \cdot 10^{-2}$ | 0.187 m                                                                                    | E = 0.920 up to $x'' = 0.190$ m<br>E = 0.282 for $x > x''$   |

FIGURE 2

COMPARISON OF THE CALCULATED STEAM TEMPERATURE  
PROFILES WITH THE EXPERIMENTAL PROFILE  
FOR A SOLUTION OF 10% NaNO<sub>3</sub> (RUN B)

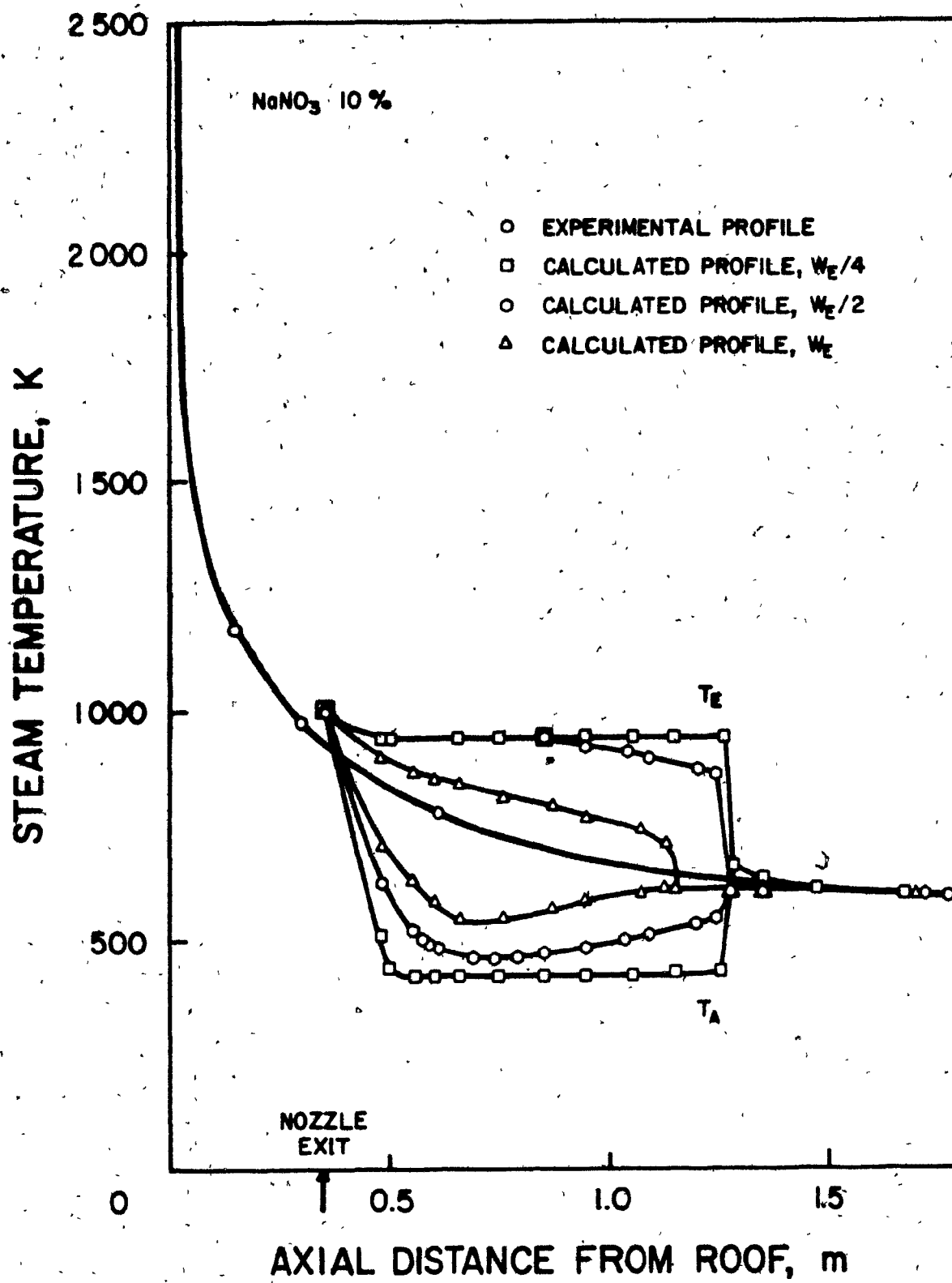
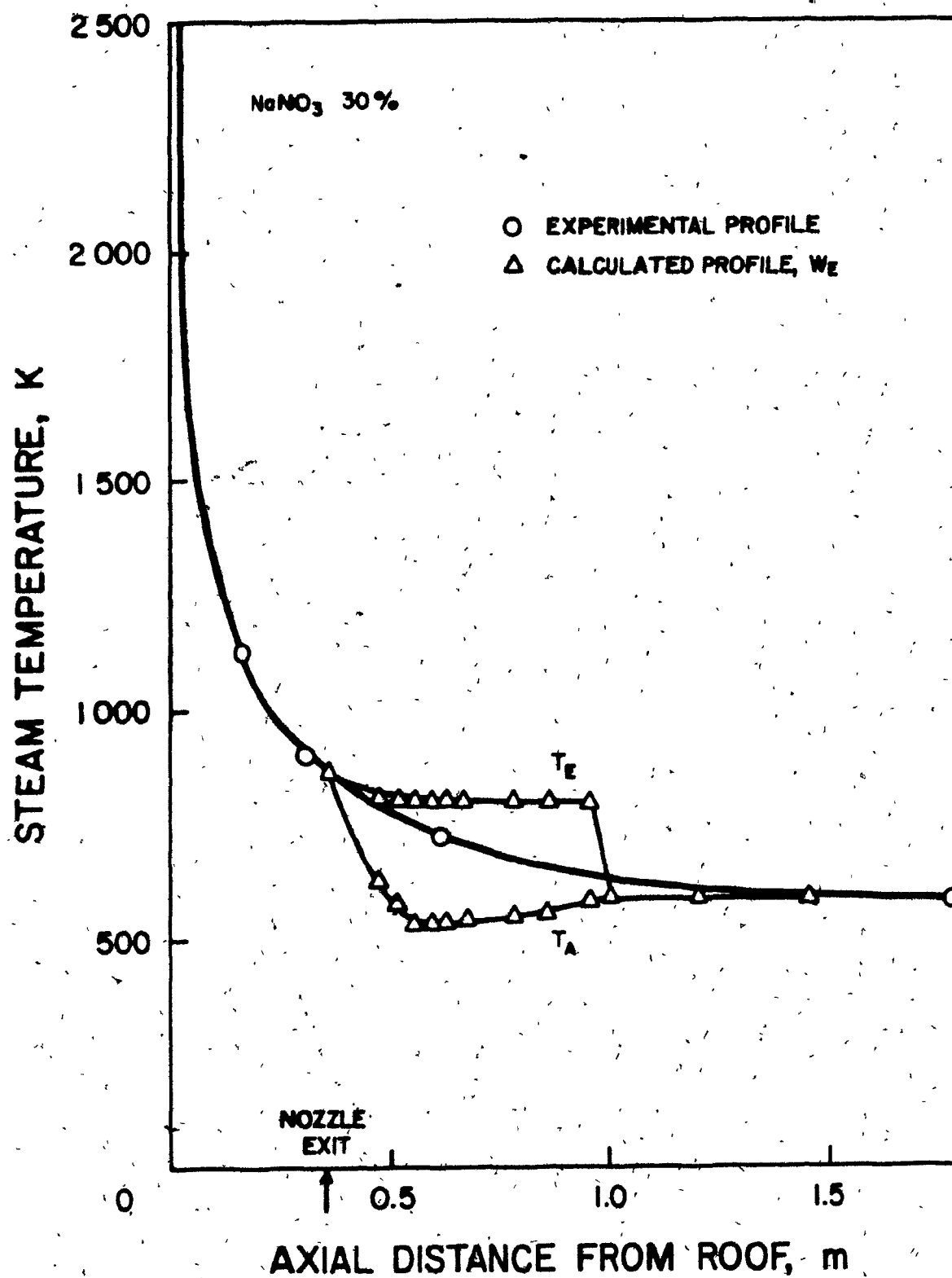


FIGURE 3

COMPARISON OF THE CALCULATED STEAM TEMPERATURE

PROFILES WITH THE EXPERIMENTAL PROFILE

FOR A SOLUTION OF 30%  $\text{NaNO}_3$  (RUN A)



high values of the entrainment rate which created instabilities within the spray jet.

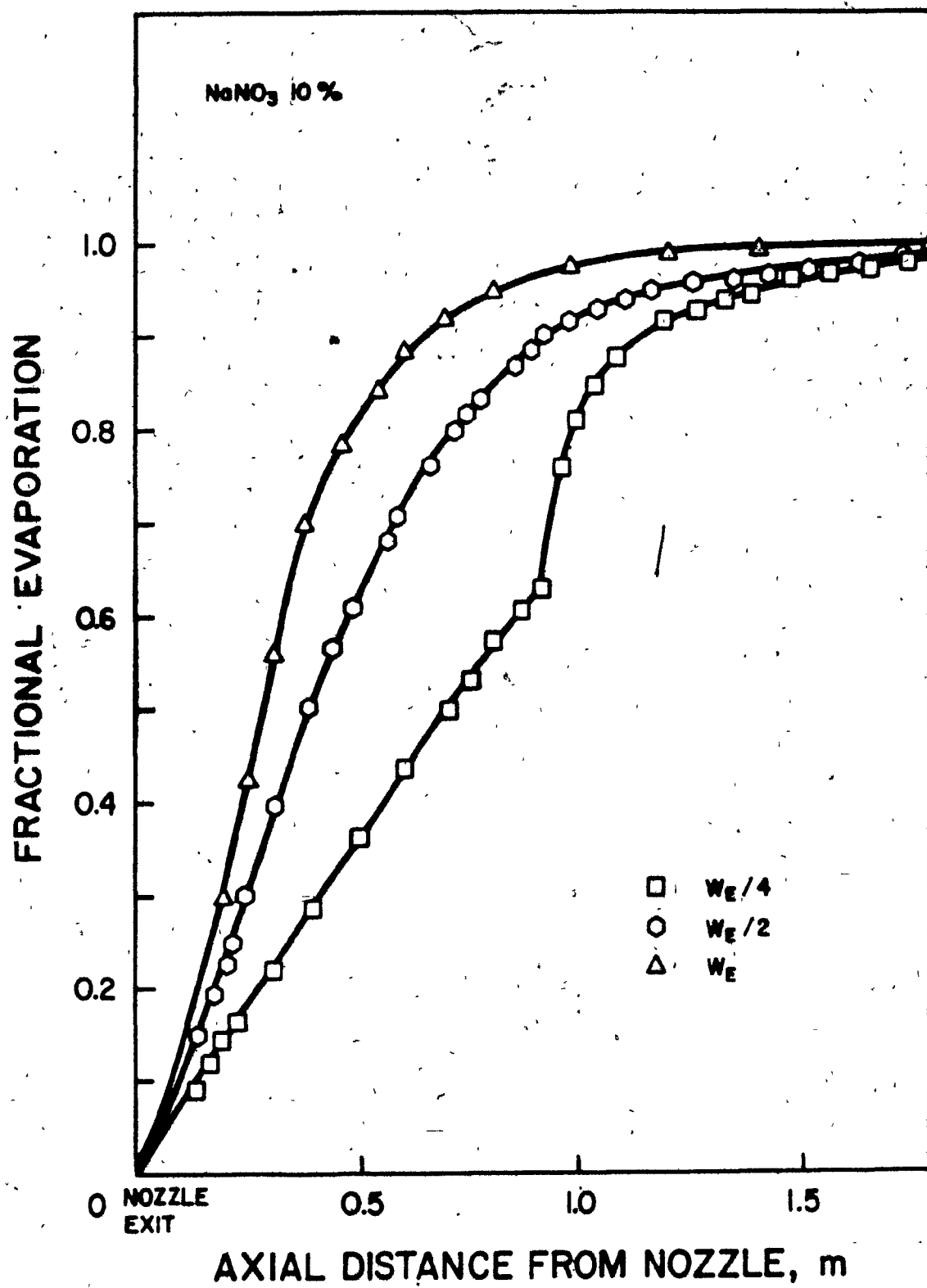
A consideration of the evaporation curves led to the same conclusions. On Figure 4 is plotted the fraction of the total dryer evaporating capacity which has occurred down to a distance  $x$  from the nozzle in the axial direction for the 10%  $\text{NaNO}_3$  solution, for several assumed rates of entrainment within the spray. When the rate of entrainment is divided by 4, the curve shows a large discontinuity of the rate of evaporation at the transition from the nozzle zone to the free-entrainment zone, whatever length the nozzle zone may have; the rate of evaporation appears to be lower in the nozzle zone than at the beginning of the free-entrainment zone, which is quite opposite to experimental evidence which supports the existence of a higher heat transfer rate in the jet: the change in the experimental steam temperature in the first 60 centimeters is about twice that which occurs in the last 80 cm of the chamber. When the rate of entrainment is divided by 2, this behavior is the same although it is less accentuated; finally, the curve corresponding to the total rate of entrainment shows the normal trend corresponding to a higher evaporation rate in the first 60 cm below the nozzle and does not present any discontinuity at all.

From these two series of graphs, one can realize the severe errors which are introduced if the jet dynamics are not well accounted for; however, the effect of the large volume changes caused by the evaporation process on the entrainment of the spray jet do not appear



FIGURE 4

FRACTIONAL EVAPORATION OF THE SPRAY AS A  
FUNCTION OF THE AXIAL DISTANCE FROM THE  
NOZZLE FOR A SOLUTION OF 10%  $\text{NaNO}_3$



to be as important as it was felt previously and it is certainly attenuated by the intense turbulence, enhanced by the small diameter of the chamber, which forces the surrounding steam into the jet. Thus, the rate of entrainment as correlated by Benatt and Eisenklam (1969) was used in the modelling, without modification.

## 2. Recirculation of the Surrounding Steam in the Dryer

There is considerable uncertainty about the flow pattern of the superheated steam surrounding the spray jet. The pumping effect of the spray induces a strong upward flow and causes mixing of steam from the colder lower regions with the highly superheated steam from the torch in the upper section of the chamber; that, together with the heat losses through the dryer walls, results in a steady decrease in temperature in the surrounding steam in the annulus from top to bottom. Moreover, from consideration of the experimental steam and wall temperature profiles, it can be inferred that the wall temperature stays close to the temperature of the steam at any point below the atomizing nozzle as long as no impingement of liquid droplets occurs, and that consequently the downward natural convection flow in the boundary layer along the dryer wall is negligible.

The recirculation phenomenon induced by the pumping effect of both the plasma jet and the spray has a large influence on the determination of the temperature gradients within the dryer; it is present in two terms in the heat balance giving the temperature  $T_E$

of the surrounding steam (Equation 11): its total enthalpy and its flow rate; and these terms are both unknown in the present work.

Gauvin (1981) had arbitrarily assumed that the amount  $\underline{W_T}$  of recirculating steam varied exponentially with the concentration of the largest droplet in the nozzle zone, between two extreme values  $\underline{W_{T_i}}$  and  $\underline{W_{T_f}}$ :

$$\underline{W_T} = \underline{W_{T_f}} - (\underline{W_{T_f}} - \underline{W_{T_i}}) e^{C_1(C_{LD} - C_i)} \quad (15)$$

where  $\underline{W_{T_i}}$  is its initial value at the nozzle level

$\underline{W_{T_f}}$  is its final value, constant throughout the entire free-entrainment zone

$\underline{C_i}$  is the initial concentration of the solution

$\underline{C_{LD}}$  is the largest droplet concentration

and  $\underline{C_1}$  a constant equal to  $\ln(0.1) / 0.02$ .

The temperature of the recirculating steam, coming directly from the bottom of the dryer, was set equal to the outlet temperature  $\underline{T_o}$  of the dryer. That model was tried on the four experimental runs with the total rate of entrainment  $\underline{W_E}$ , and it always resulted in a temperature profile of the drying steam,  $\underline{T_A}$ , which was much too low compared to the experimental profile (Figures 2 and 3). Besides the amount  $\underline{W_E}$  of steam entrained within the spray up to a certain distance  $\underline{x}$  from the nozzle appeared to be inevitably larger than the total mass of surrounding steam present in the same control volume,

for distances  $x$  corresponding to the last portion of the nozzle zone; this was opposite to the law of mass conservation which supposes that  $\underline{W}_E$  is at the very most equal to the amount of surrounding steam contained in the annulus corresponding to the control volume; it certainly explains why the amount of entrainment was divided by 4 in the previous study as there was no experimental evidence available to contradict that reduction.

The surrounding steam in the annulus is composed of superheated steam coming from the torch and of recirculating steam coming from a lower region. The "plasma-superheated steam" mass flow rate,  $\underline{W}_G$ , has indeed a fixed value determined by the following overall heat balance:

$$\underline{W}_G H_G + \underline{W}_F H_F = \underline{W}_O H_O + q_L \quad (16)$$

in which the enthalpy of the product is neglected. Since the amount of entrainment within the spray is also known by the correlation of Benatt and Eisenklam (1969), only the amount of recirculating steam can be modified to satisfy mass conservation. At the nozzle level, the amount of recirculation is equal to its initial value  $\underline{W}_{T1}$ , which is the amount of entrainment within the expanding plasma jet, required to produce the steep axial temperature gradient recorded in the mixing zone, above the atomizing nozzle (see Figure 1). Then, at a distance  $x$  from the nozzle, the amount of recirculation  $\underline{W}_T$  is assumed to be exactly the sum of its initial value  $\underline{W}_{T1}$  and of  $\underline{W}_E$  which is the cumulated amount of steam entrained within the spray up to the distance  $x$ :

$$W_T = W_{T_1} + W_E \quad (17)$$

Let us consider the assumption that the temperature of the recirculating steam is at any point equal to the outlet temperature of the dryer  $T_o$ ; the term  $W_T H_T$  then introduces a considerable temperature drop within the surrounding steam in the first centimeters of the nozzle zone. The drying steam temperature  $T_A$  within the spray becomes then higher than the temperature  $T_E$  of the steam surrounding the spray, which is obviously wrong. Since the circulating steam in the annulus must obviously get hotter as it goes upwards, it is now assumed that the temperature profile of the heavily-insulated wall is a fairly good indication of the recirculating steam temperature profile within the nozzle zone, based on the experimental evidence that the wall and steam temperatures are relatively close together; the chamber wall, made of stainless steel of low thermal conductivity, is thus supposed to give a measure of the temperature of the steam going along at any point, as long as impingement of droplets does not occur on it.

Parabolic equations can be derived, using the "least-square method", to fit the experimental wall temperature profiles in the upper section of the chamber for the different runs; the abscissa origin is set at the nozzle level and the analytical expression thus derived is valid to describe the recirculating steam temperature profile until it reaches the outlet temperature of the dryer  $T_o$ ; that condition of continuity determines the end of the nozzle zone, since at any point within the free-entrainment zone, the recirculating steam is coming directly from the bottom of the chamber at the outlet temperature  $T_o$ .

because of the high intensity of turbulence prevailing in that region.

For both runs with pure water, no such temperature profile could be directly determined from the experimental wall temperature profile, since the walls were cooled, starting at a small distance from the nozzle, by the continuous impingement of cold water droplets from the atomized spray; this is particularly evident from the run with the nozzle LNN2, which has a large spray-cone angle, where the temperature of the wall is cooled to the boiling point temperature of water almost immediately in the region of the nozzle exit. In these two cases, the wall temperature profile obtained for the solution of 30%  $\text{NaNO}_3$  can be an acceptable approximation of the recirculating steam temperature profile, when a shift of some degrees has been made to account for the different conditions of temperature at the nozzle level. This is confirmed by the following results: using the profile obtained for the solution of 30%  $\text{NaNO}_3$ , with a shift of 70 degrees Kelvin (that is, the experimental temperatures for 30%  $\text{NaNO}_3$  were raised by 70 K) in the simulation of the operation with 10%  $\text{NaNO}_3$ , gives about the same results as when a specific equation is derived for the actual wall temperature profile for the 10%  $\text{NaNO}_3$  solution (Figure 5). The total length of the chamber required for drying differs by only 1.5% in these two approaches.

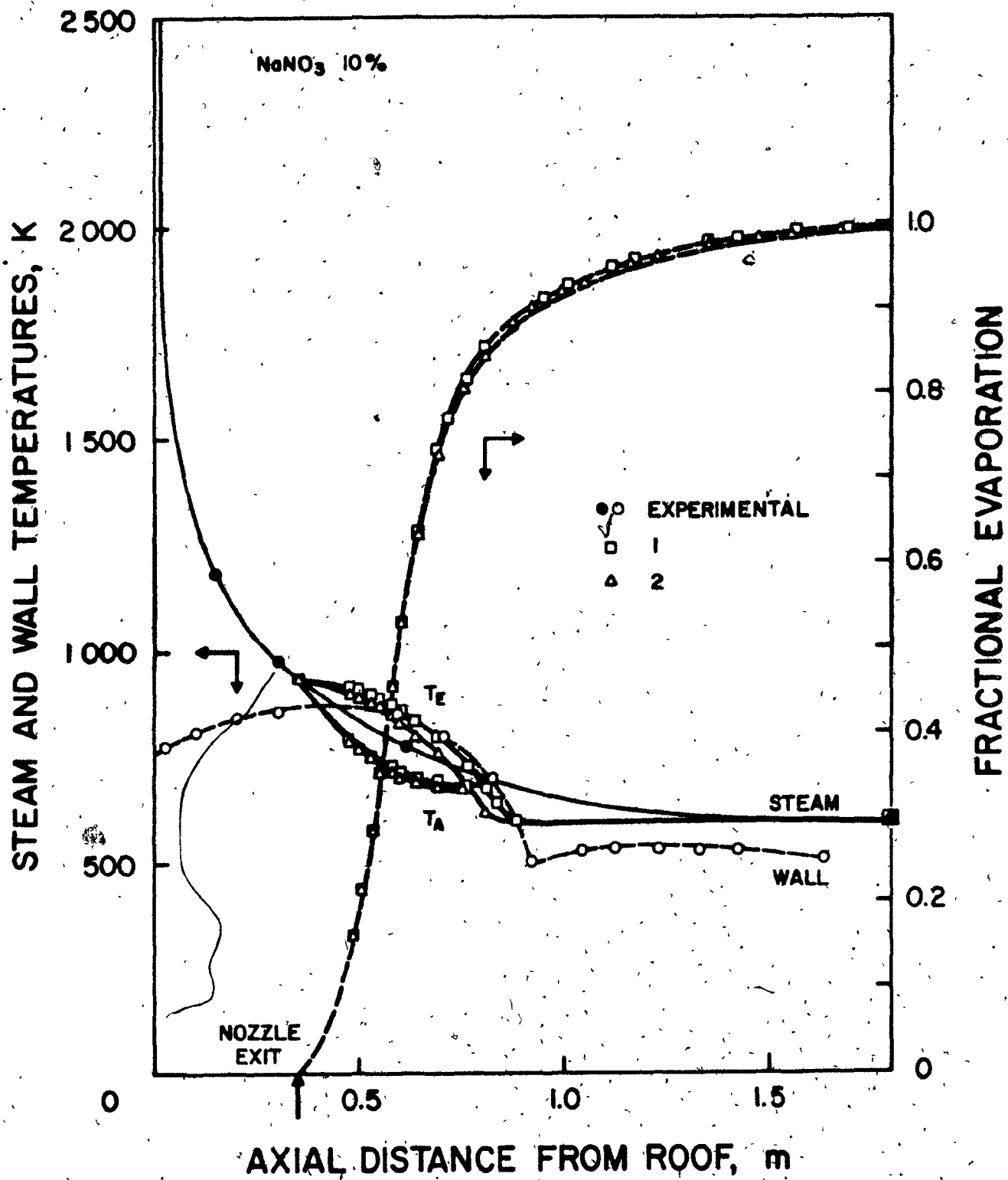
It may then be concluded from the above that this modelling can be successfully used, provided that an analytical approximation of the recirculating steam temperature profile is known from some

FIGURE 5

COMPARISON OF THE CALCULATED STEAM TEMPERATURE  
PROFILES WITH THE EXPERIMENTAL PROFILE FOR  
THE SOLUTION OF 10%  $\text{NaNO}_3$ , USING THE RECIRCULATING  
STEAM TEMPERATURE PROFILE DERIVED FROM:

1. THE ACTUAL WALL TEMPERATURE PROFILE FOR THE  
SOLUTION OF 10%  $\text{NaNO}_3$
2. THE WALL TEMPERATURE PROFILE CORRESPONDING TO  
THE SOLUTION OF 30%  $\text{NaNO}_3$ , SHIFTED UPWARD BY  
70 K





experimental evidence or correlation. The equations used in the four experimental cases studied are listed in Table II, together with the corresponding length of each nozzle zone.

### 3. Steam Flow Pattern Within the Dryer

A zero tangential velocity can be assumed for the steam and the droplets in the whole chamber as a consequence of the dryer design and of the use of swirlless pressure atomizing nozzles. The simulation is then two-dimensional, involving axial and radial motions of the gas and droplets.

The radial velocity of the entrained steam is set equal to zero within the spray, on the basis of the work carried out on jets by Benatt and Eisenklam (1969); furthermore, it is assumed to be zero anywhere else in the chamber, in the absence of any adequate correlation for the present chamber design.

The axial velocity of steam is correlated by the semi-empirical equation of Pai (1954) in the nozzle zone:

$$V_v = V_c \exp [-0.692 (r/r_5)^2] \quad (18)$$

where  $V_c = 3.2 V_n D_n / x \quad (19)$

and  $r_5 = x \tan (\theta/2) \quad (20)$

That equation has been used to correlate experimental data from several authors and it happened to fit these data fairly well.

TABLE II

RECIRCULATING STEAM TEMPERATURE  
PROFILES IN THE NOZZLE ZONE

|                                | Analytical expression approximating the<br>recirculating steam temperature profile | Length of the<br>nozzle zone |
|--------------------------------|------------------------------------------------------------------------------------|------------------------------|
| Run A<br>30% NaNO <sub>3</sub> | $T_T = - 1141 x^2 - 31.7 x + 794$                                                  | 0.400 m ( $T_T > 590$ K)     |
| Run B<br>10% NaNO <sub>3</sub> | $T_T = - 1114 x^2 + 38 x + 883$                                                    | 0.530 m ( $T_T > 590$ K)     |
| Run C<br>Pure Water-Monarch    | $T_T = - 1141 x^2 - 31.7 x + 794$                                                  | 0.440 m ( $T_T > 560$ K)     |
| Run D<br>Pure Water-LNN2       | $T_T = - 1141 x^2 - 31.7 x + 694$                                                  | 0.300 m ( $T_T > 560$ K)     |

In the absence of an adequate correlation for the axial steam velocity in the free-entrainment zone where good mixing occurs, plug flow is assumed and the axial velocity of steam at a distance  $x$  from the nozzle is thus given by:

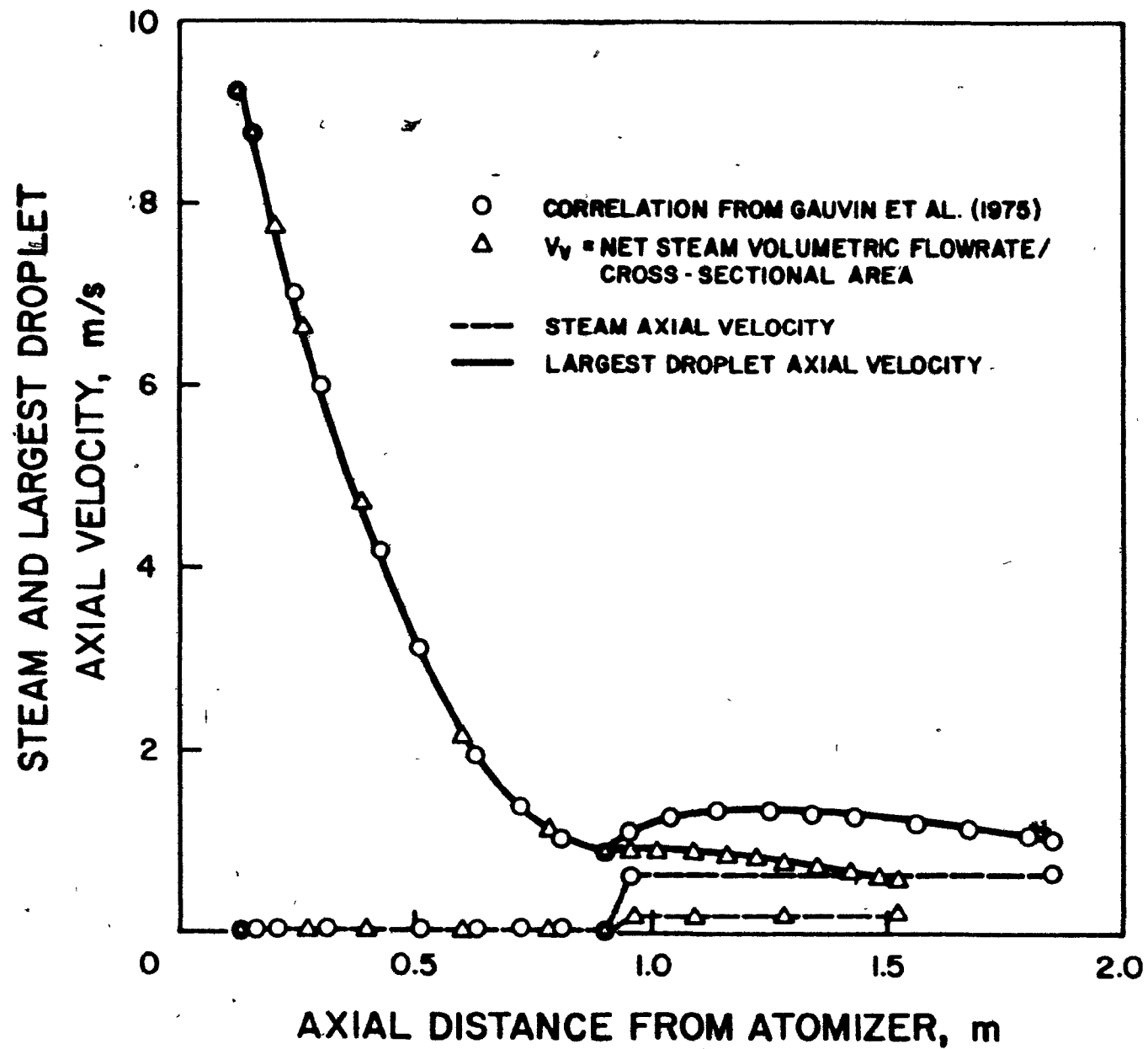
$$V_v = (W_G + E_x) / \rho_G \pi R^2 \quad (21)$$

where  $(W_G + E_x) / \rho_G$  is the net volumetric flow rate of the steam at the abscissa  $x$ .

The curves for the velocity of the drying steam and of the largest droplet axial velocity as a function of the axial distance from the atomizer nozzle are shown in Figure 6 for the 10%  $\text{NaNO}_3$  solution (Run B). The velocity of the largest droplet was calculated according to the method of Gauvin et al. (1975). These were calculated on the basis of the plug flow assumption for the steam velocity, as given in Equation (21). The axial velocity of the droplets (issuing from the atomizer at velocities between 50 and 60 m/S) decreases very sharply with distance in the nozzle zone. Identical values are calculated in that zone for the two methods of estimating the steam velocity. In the free-entrainment zone, however, (about 0.9 metre from the atomizer on Figure 6) the plug flow assumption provides a smooth transition in the velocity of the droplet, which are now very near their terminal velocity, whereas the method of Gauvin et al. (1975) shows an increase in the droplet velocity which is difficult to reconcile with the physical situation. It is highly probable, therefore, that the assumption of plug flow in the free-entrainment zone is reasonable.

FIGURE 6

STEAM AND LARGEST DROPLET  
CALCULATED AXIAL VELOCITY PROFILES  
FOR THE SOLUTION OF 10%  $\text{NaNO}_3$  (RUN B)



#### 4. New Criterion of Drying

All the previous simulations by Gauvin and co-workers have been based on the criterion that the largest particle reaches a given residual moisture content, usually before it hits the dryer wall.

The criterion used to determine the final point of the present calculations is different: the simulation is allowed to proceed until the largest droplet is dry. This corresponds well to the conditions which were used in the experimental work.

However, since the results are well detailed, any other criterion can be chosen within the simulation to obtain the dimensions of a spray dryer which would be more realistic from an industrial point of view.

#### SPECIFICATIONS REQUIRED FOR THE PROGRAM

In this section are presented all the variables which are needed as input data in the computer program, for the four experimental operations with sodium nitrate and water which were studied. These variables are classified into three basic groups, as shown in Table III.

1. The characteristic features of the drying operation such as plasma characteristics at the exit of the torch, important steam temperatures within the chamber, nature of feed, feed flow rate and concentration, heat losses through the chamber walls, etc.

2. The atomizing nozzle specifications, together with the drop size distribution generated.

**TABLE III**  
**SPECIFICATIONS OF THE FOUR DIFFERENT DRYING OPERATIONS**

|                                           | A                          | B                           | C                          | D.                         |
|-------------------------------------------|----------------------------|-----------------------------|----------------------------|----------------------------|
| <b>1. Drying Operation Specifications</b> |                            |                             |                            |                            |
| Nature of Feed                            | NaNO <sub>3</sub>          | NaNO <sub>3</sub>           | Water                      | Water                      |
| Feed Concentration, %                     | 30                         | 10                          | 2                          | 2                          |
| Feed Max. Rate, kg/s                      | 0.506 10 <sup>-2</sup>     | 0.431 10 <sup>-2</sup>      | 0.416 10 <sup>-2</sup>     | 0.403 10 <sup>-2</sup>     |
| Plasma Steam Temperature, K               | 3580                       | 3615                        | 3590                       | 3580                       |
| Plasma Enthalpy at Torch Exit, J/kg       | 3.19 10 <sup>7</sup>       | 3.34 10 <sup>7</sup>        | 3.19 10 <sup>7</sup>       | 3.19 10 <sup>7</sup>       |
| Plasma Temperature at Atomizer, K         | 860                        | 930                         | 900                        | 770                        |
| T <sub>o</sub> , Outlet Temperature, K    | 590                        | 590                         | 560                        | 560                        |
| T <sub>f</sub> , Feed Temperature, K      | 283                        | 283                         | 283                        | 283                        |
| Heat Losses, %                            | 12                         | 10                          | 3                          | 5                          |
| Chamber Pressure, 10 <sup>5</sup> Pa      | 1 bar                      | 1 bar                       | 1 bar                      | 1 bar                      |
| Radius of Chamber, m                      | 0.15                       | 0.15                        | 0.15                       | 0.15                       |
| Length of Nozzle Zone, m                  | 0.400                      | 0.530                       | 0.440                      | 0.300                      |
| <b>2. Nozzle Specifications</b>           |                            |                             |                            |                            |
| Pressure Nozzle Type                      | Solid Cone                 | Solid Cone                  | Solid Cone                 | Hollow Cone                |
| Nozzle Make                               | Monarch HV 4.5             | Monarch HV 4.5              | Monarch HV 4.0             | Spraying Systems, LNN2     |
| Atomizing Pressure, Pa (psia)             | 2.17 10 <sup>6</sup> (315) | 0.965 10 <sup>6</sup> (140) | 1.17 10 <sup>6</sup> (165) | 1.41 10 <sup>6</sup> (205) |
| Orifice Diameter, m                       | 0.559 10 <sup>-3</sup>     | 0.559 10 <sup>-3</sup>      | 0.533 10 <sup>-3</sup>     | 0.711 10 <sup>-3</sup>     |
| 1/2 Angle of Spray                        | 15°                        | 15°                         | 15°                        | 40°                        |
| Liquid Velocity at Nozzle Exit, m/s       | 60                         | 43                          | 48                         | 58                         |
| Drop Size Distribution                    | Table IV                   | Table V                     | Table VI                   | Table VII                  |
| Largest Droplet Diameter, m               | 176 10 <sup>-6</sup>       | 226 10 <sup>-6</sup>        | 225 10 <sup>-6</sup>       | 180 10 <sup>-6</sup>       |
| Mean Sauter Diameter, m                   | 73 10 <sup>-6</sup>        | 101 10 <sup>-6</sup>        | 110 10 <sup>-6</sup>       | 77 10 <sup>-6</sup>        |
| <b>3. Material Characteristics</b>        |                            |                             |                            |                            |
| Feed Density, kg/m <sup>3</sup>           | 1230                       | 1070                        | 1000                       | 1000                       |
| Solid Heat Capacity, J/kg K               | 1640                       | 1640                        | 1640                       | 1640                       |
| B.P. at Saturation (T <sub>w</sub> ), K   | 392                        | 392                         | 373                        | 373                        |
| Latent Heat of Vaporization, J/kg         | 0.220 10 <sup>7</sup>      | 0.220 10 <sup>7</sup>       | 0.226 10 <sup>7</sup>      | 0.226 10 <sup>7</sup>      |



3. The feed material properties such as its density, its heat capacity, its boiling point at saturation and the corresponding latent heat of vaporization.

All the values listed in Table III but two, are the actual experimental values corresponding to the maximum evaporative capacity of the chamber, for each concentration of salt. These two values which were neither measured nor given by other experimental data, are the length of the nozzle zone and the average velocity of the liquid feed at the exit of the atomizing nozzle.

The length of the nozzle zone is determined by the condition of continuity on the temperature of the recirculating steam, establishing that it has to be higher in the nozzle zone than its final value  $T_o$  in the free-entrainment zone (Table III). That value is as arbitrary as the analytical expression approximately the recirculating steam temperature profile and should instead have been experimentally measured.

The average velocity of the liquid feed at the exit of the swirlless pressure nozzle is computed from Masters (1979):

$$V_n = K_v \sqrt{2P/\rho_{\text{feed}}} \quad (22)$$

According to Dombrowski and Wolfsohn (1972), at high atomizing pressures and small spray angles, the velocity coefficient  $K_v$  is about 0.8 to 0.9. It is interesting to note that a change of more than 100% in that velocity induces only a very slight variation

(about 8%) of the total length (and time) required for drying. Therefore, an order of magnitude obtained with Equation (22) will be amply sufficient for the present simulation and the velocity coefficient is thus taken equal to one in that Equation.

The simulation cannot be used directly for pure water (Runs C and D) since the droplets are declared dried in the simulation when they reach a concentration of 100% of solids. When droplets of pure water are being dried, they evaporate away completely and the computer program cannot describe such a behaviour, unless considerable changes are made in it. There is little interest in the description of a system for drying water and, as a first approximation, it seems adequate to simulate the drying of water by considering the drying of a very dilute solution of  $\text{NaNO}_3$ ; thus, a very small initial concentration of solids (2% by wt) is assumed in Runs C and D. Moreover, as the droplets remain at a very low concentration of solids during the major part of the drying process, the surface of the droplets is assumed to remain at the boiling point of water (373 K) and all properties are those of water. However, the solid heat capacity is retained, thus allowing subsequent heating of the small amount of solid after the drying is completed.

The important runs to test the simulation procedure are of course those with 10%  $\text{NaNO}_3$  and 30%  $\text{NaNO}_3$  solutions as feed (Runs A and B). Not much importance will be attached to the water runs, except for the purpose of comparing the influence of very different atomizing conditions.

The Prandtl Number in the heat transfer equation is taken equal to unity as a reasonable value for turbulent flow of highly superheated water vapor.

The time increments are carefully chosen to give workable printouts: the interval is set at 0.0005 or 0.001 second for the first 0.06 second; it is then set at 0.002 second until a total time of 0.30 second is reached, at which point it is set at 0.01 second. In order to take into account the effect of the higher temperatures in the vicinity of the nozzle, calculations start at a distance  $x$  of 0.1 m from the latter. At that position, the variables are set equal to their calculated or experimental initial values. Since this initial position may have an important bearing on the results, it was decided to test this assumption by taking smaller and higher values, specifically 0.04 m and 0.25 m, in two different sets of calculations, all other operating conditions being otherwise the same; it resulted in a variation of less than 10% in the total length and time required for drying; therefore, the value of 0.1 m was finally retained as quite acceptable.

#### Drop Size Distribution

A small computer program was written to generate the four drop size distributions introduced in the main program, from the cumulative number frequency curves established experimentally (Figures 14, 17, 20 and 24, Part I of the Experimental Section). For that purpose, the program calculated first the cumulative volume

frequency distribution and determined by interpolation the droplets corresponding to 0%, 10%, 20%, 30%, ...., and 100% on that volume frequency curve; next, the exact volume frequencies were calculated for the classes which were thus defined. The upper two classes were further subdivided to give more detailed information on the drying of the largest droplets since the latter have a great influence on the total length (and time) required for drying. A cut-off minimum diameter was calculated for these distributions so that all drops smaller than that diameter are disregarded in the distribution; it is defined as the diameter of the smallest droplet which is large enough so that its impaction efficiency on the sampling device is 100%. The exact calculation, corresponding to the same sampling device as the one used in the present work, has been done in Baltas's thesis (1967) (Appendix B) and it gave 20  $\mu\text{m}$  for unevaporated droplets of 30%  $\text{NaNO}_3$ . That cut-off diameter was used in the present analysis even in the drop size distributions for pure water, since the exact value may not be very different from 20  $\mu\text{m}$  - it is some function of the liquid density only - and that disregarded volume would be in any case too small a fraction of the total volume to affect considerably the overall volume distribution.

The resulting volume frequency distributions are then formed of 12 or 13 classes of droplets and each class is represented by its median diameter, except the last one which is represented by the largest droplet diameter for the reason given above for the subdivision of classes. These distributions are presented in

Tables IV, V, VI and VII for the four operations experimentally studied; the largest droplet diameters given by Mugele and Evans (1951) upper-limit function are also reported in Table III and they appear to be in excellent agreement with the experimental results reported in Tables IV to VII.

#### SIMULATION RESULTS

The results of the simulations of the four experimental runs are listed in the following four Tables, together with the corresponding experimental values, in order to provide easier comparison.

For each set of results, the table is followed by a figure showing the experimentally-determined and the calculated temperature profiles for the chamber walls and for the steam within the chamber, and also the calculated fraction of the total evaporation, as a function of the axial distance from the roof of the chamber.

The listing of the program, together with the output results for the simulation of the solution of 30% NaNO<sub>3</sub> is given in Appendix E.

TABLE IV  
DROP SIZE DISTRIBUTION  
FOR THE 30% NaNO<sub>3</sub> SOLUTION  
 (RUN A)

| Class Number | Class Limits<br>( $\mu\text{m}$ ) | Median Diameter<br>( $\mu\text{m}$ ) | Volume Relative Frequency<br>(%) | Volume Cumulative Frequency<br>(%) |
|--------------|-----------------------------------|--------------------------------------|----------------------------------|------------------------------------|
| 1            | 20.0 - 43.8                       | 29.0                                 | 14.16                            | 14.16                              |
| 2            | 43.8 - 53.1                       | 49.0                                 | 5.08                             | 19.24                              |
| 3            | 53.1 - 63.3                       | 58.7                                 | 11.02                            | 30.26                              |
| 4            | 63.3 - 75.8                       | 71.8                                 | 10.19                            | 40.45                              |
| 5            | 75.8 - 93.1                       | 84.3                                 | 9.10                             | 49.54                              |
| 6            | 93.1 - 106.0                      | 98.1                                 | 6.80                             | 56.34                              |
| 7            | 106.0 - 116.0                     | 109.0                                | 14.48                            | 70.82                              |
| 8            | 116.0 - 126.1                     | 121.3                                | 9.82                             | 80.64                              |
| 9            | 126.1 - 143.6                     | 135.0                                | 7.45                             | 88.09                              |
| 10           | 143.6 - 144.0                     | 143.8                                | 6.20                             | 94.29                              |
| 11           | 144.0 - 161.7                     | 150.0                                | 1.28                             | 95.57                              |
| 12           | 161.7 - 166.2                     | 164.0                                | 3.35                             | 98.93                              |
| 13           | 166.2 - 178.1                     | 178.1                                | 1.07                             | 100.00                             |

TABLE V

DROP SIZE DISTRIBUTION  
FOR THE 10% NaNO<sub>3</sub> SOLUTION  
(RUN B)

| Class Number | Class Limits<br>( $\mu\text{m}$ ) | Median Diameter<br>( $\mu\text{m}$ ) | Volume Relative Frequency<br>(%) | Volume Cumulative Frequency<br>(%) |
|--------------|-----------------------------------|--------------------------------------|----------------------------------|------------------------------------|
| 1            | 20.0 - 63.9                       | 42.4                                 | 10.27                            | 10.27                              |
| 2            | 63.9 - 86.2                       | 75.5                                 | 9.73                             | 20.00                              |
| 3            | 86.2 - 94.1                       | 92.2                                 | 13.38                            | 33.38                              |
| 4            | 94.1 - 101.4                      | 100.0                                | 6.68                             | 40.06                              |
| 5            | 101.4 - 112.6                     | 109.6                                | 13.21                            | 53.26                              |
| 6            | 112.6 - 124.0                     | 119.0                                | 4.42                             | 57.68                              |
| 7            | 124.0 - 137.3                     | 126.1                                | 12.00                            | 69.68                              |
| 8            | 137.3 - 156.3                     | 147.3                                | 14.04                            | 83.72                              |
| 9            | 156.3 - 186.5                     | 169.0                                | 5.99                             | 89.71                              |
| 10           | 186.5 - 187.8                     | 187.5                                | 5.22                             | 94.93                              |
| 11           | 187.8 - 217.5                     | 206.3                                | 2.31                             | 97.24                              |
| 12           | 217.5 - 218.75                    | 218.8                                | 2.76                             | 100.00                             |

TABLE VI  
DROP SIZE DISTRIBUTION  
FOR PURE WATER  
(RUN C - NOZZLE MONARCH)

| Class<br>Number | Class Limits<br>( $\mu\text{m}$ ) | Median<br>Diameter<br>( $\mu\text{m}$ ) | Volume Relative<br>Frequency<br>(%) | Volume Cumulative<br>Frequency<br>(%) |
|-----------------|-----------------------------------|-----------------------------------------|-------------------------------------|---------------------------------------|
| 1               | 20.0 - 66.2                       | 35.6                                    | 10.21                               | 10.21                                 |
| 2               | 66.2 - 86.9                       | 75.9                                    | 7.99                                | 18.20                                 |
| 3               | 86.9 - 100.1                      | 94.0                                    | 12.96                               | 31.15                                 |
| 4               | 100.1 - 117.9                     | 110.6                                   | 6.99                                | 38.14                                 |
| 5               | 117.9 - 126.1                     | 122.3                                   | 13.75                               | 51.90                                 |
| 6               | 126.1 - 150.0                     | 135.0                                   | 6.56                                | 58.46                                 |
| 7               | 150.0 - 168.6                     | 155.0                                   | 8.77                                | 67.23                                 |
| 8               | 168.6 - 186.4                     | 174.0                                   | 11.19                               | 78.41                                 |
| 9               | 186.4 - 199.8                     | 190.7                                   | 9.33                                | 87.74                                 |
| 10              | 199.8 - 230.4                     | 200.0                                   | 3.59                                | 91.33                                 |
| 11              | 230.4 - 231.3                     | 231.3                                   | 4.16                                | 95.49                                 |
| 12              | 231.3 - 237.5                     | 237.5                                   | 4.51                                | 100.00                                |



TABLE VII  
DROP SIZE DISTRIBUTION  
FOR PURE WATER  
(RUN D - NOZZLE LNN2)

| <u>Class<br/>Number</u> | <u>Class Limits<br/>(<math>\mu</math>m)</u> | <u>Median<br/>Diameter<br/>(<math>\mu</math>m)</u> | <u>Volume Relative<br/>Frequency<br/>(%)</u> | <u>Volume Cumulative<br/>Frequency<br/>(%)</u> |
|-------------------------|---------------------------------------------|----------------------------------------------------|----------------------------------------------|------------------------------------------------|
| 1                       | 20.0 - 49.9                                 | 26.8                                               | 9.05                                         | 9.05                                           |
| 2                       | 49.9 - 62.4                                 | 53.7                                               | 10.10                                        | 19.15                                          |
| 3                       | 62.4 - 69.6                                 | 63.9                                               | 12.26                                        | 31.41                                          |
| 4                       | 69.6 - 80.6                                 | 75.0                                               | 6.20                                         | 37.61                                          |
| 5                       | 80.6 - 87.6                                 | 84.8                                               | 13.51                                        | 51.12                                          |
| 6                       | 87.6 - 94.2                                 | 93.7                                               | 9.23                                         | 60.35                                          |
| 7                       | 94.2 - 112.4                                | 102.0                                              | 8.63                                         | 69.98                                          |
| 8                       | 112.4 - 131.9                               | 121.0                                              | 11.53                                        | 80.51                                          |
| 9                       | 131.9 - 174.6                               | 153.0                                              | 8.34                                         | 88.85                                          |
| 10                      | 174.6 - 175.3                               | 175.0                                              | 5.00                                         | 93.86                                          |
| 11                      | 175.3 - 206.3                               | 206.0                                              | 6.14                                         | 100.00                                         |

TABLE VIII  
RESULTS FOR 30% NaNO<sub>3</sub> - RUN A

|                                                 | Simulation<br>Results | Experimental<br>Conditions              |
|-------------------------------------------------|-----------------------|-----------------------------------------|
| Radius of Cylindrical Chamber                   | 0.157 m               | 0.150 m                                 |
| Height of Cylindrical Chamber<br>Below Atomizer | 1.01 m                | 1.42 m                                  |
| Corresponding Residence<br>Time of Steam        | 1.46 s                |                                         |
| Outlet Temperature                              | 590 K                 | 590 K                                   |
| Maximum Feed Flow Rate                          | 18.2 kg/h             | 18.2 kg/h                               |
| Maximum Rate of Water<br>Evaporation            | 12.75 kg/h            | 12.75 kg/h                              |
| Maximum Rate of Powder<br>Production            | 5.47 kg/h             | 5.47 kg/h                               |
| Flow Rate of "Plasma Steam"                     | 1.33 kg/h             | 1.25 kg/h                               |
| Heat Transferred to the<br>Feed Components      | 11.14 kW              | 11.3 kW<br>(11.15 + 0.15 <sup>1</sup> ) |
| Heat Losses Through the<br>Chamber Walls        | 1.11 kW               | 1.35 kW                                 |
| Effective Power Required<br>by Plasma           | 12.25 kW              | 12.65 kW                                |
| Power Required by<br>Plasma, Gross              | 17.5 kW <sup>2</sup>  | 24.7 kW                                 |

<sup>1</sup> Heat transferred to low-temperature steam fed to plasma torch.

<sup>2</sup> Based on manufacturer's specification of 70% efficiency for their water vapor plasma torch.

FIGURE 7

30% NaNO<sub>3</sub> SOLUTION

RUN A

EXPERIMENTAL AND CALCULATED

TEMPERATURE PROFILES

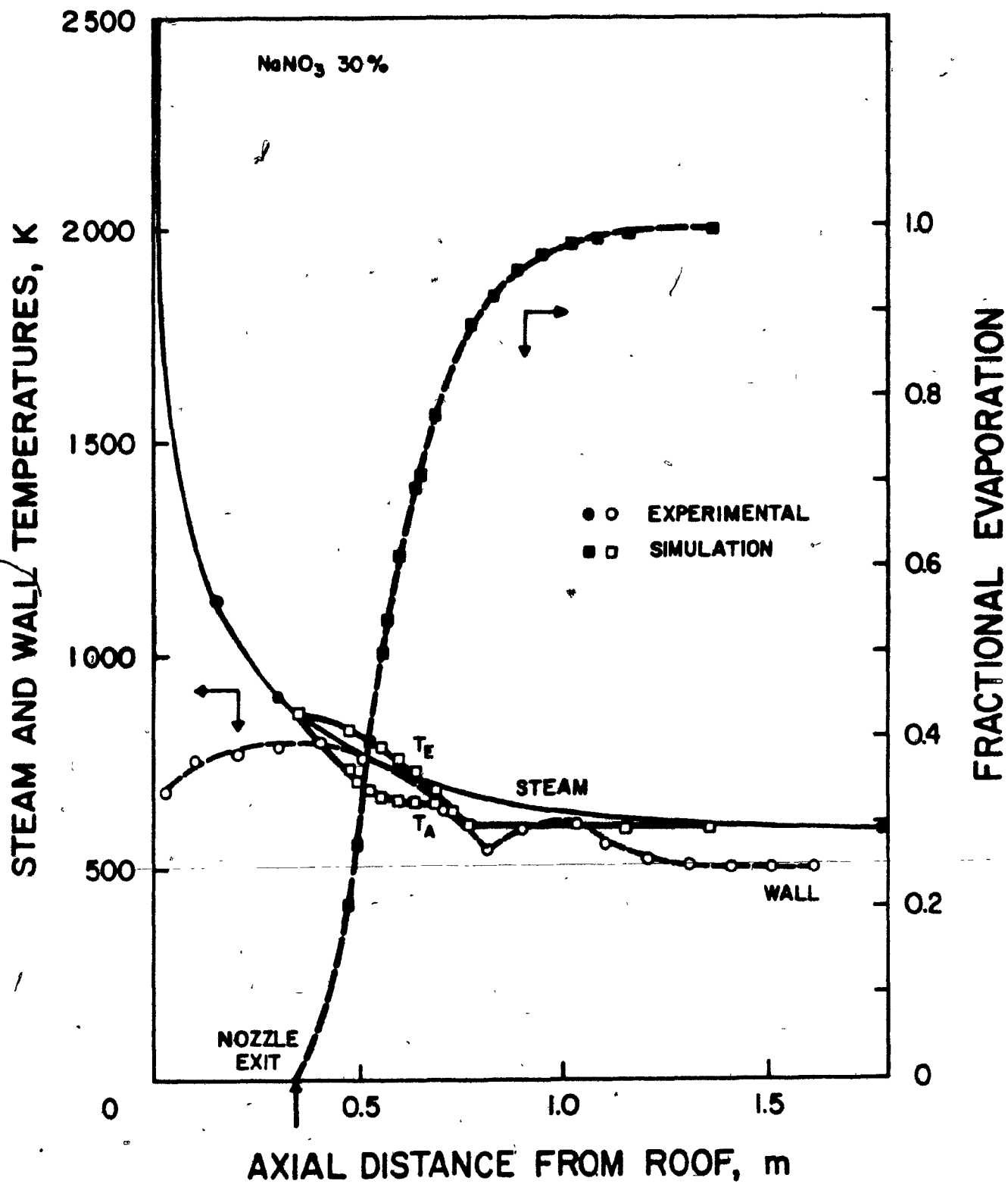


TABLE IX  
RESULTS FOR 10% NaNO<sub>3</sub> - RUN B

|                                                   | Simulation<br>Results | Experimental<br>Conditions              |
|---------------------------------------------------|-----------------------|-----------------------------------------|
| Radius of Cylindrical Chamber                     | 0.188 m               | 0.150 m                                 |
| Height of Cylindrical Chamber<br>Below Atomizer   | 1.55 m                | 1.42 m                                  |
| Corresponding Residence<br>Time of Steam          | 2.70 s                |                                         |
| Outlet Temperature                                | 590 K                 | 590 K                                   |
| Maximum Feed Flow Rate                            | 15.5 kg/h             | 15.5 kg/h                               |
| Maximum Rate of Water<br>Evaporation              | 14.0 kg/h             | 14.0 kg/h                               |
| Maximum Rate of Powder <sup>*</sup><br>Production | 1.55 kg/h             | 1.55 kg/h                               |
| Flow Rate of "Plasma Steam"                       | 1.37 kg/h             | 1.25 kg/h                               |
| Heat Transferred to the<br>Feed Components        | 11.94 kW              | 12.05 kW<br>(11.9 + 0.15 <sup>1</sup> ) |
| Heat Losses Through the<br>Chamber Walls          | 1.19 kW               | 1.35 kW                                 |
| Effective Power Required<br>by Plasma             | 13.13 kW              | 13.4 kW                                 |
| Power Required by<br>Plasma, Gross                | 18.8 kW <sup>2</sup>  | 26.0 kW                                 |

<sup>1</sup> Heat transferred to low-temperature steam fed to plasma torch.

<sup>2</sup> Based on manufacturer's specification of 70% efficiency for their water vapor plasma torch.

FIGURE 8

10% NaNO<sub>3</sub> SOLUTION

RUN B

EXPERIMENTAL AND CALCULATED

TEMPERATURE PROFILES

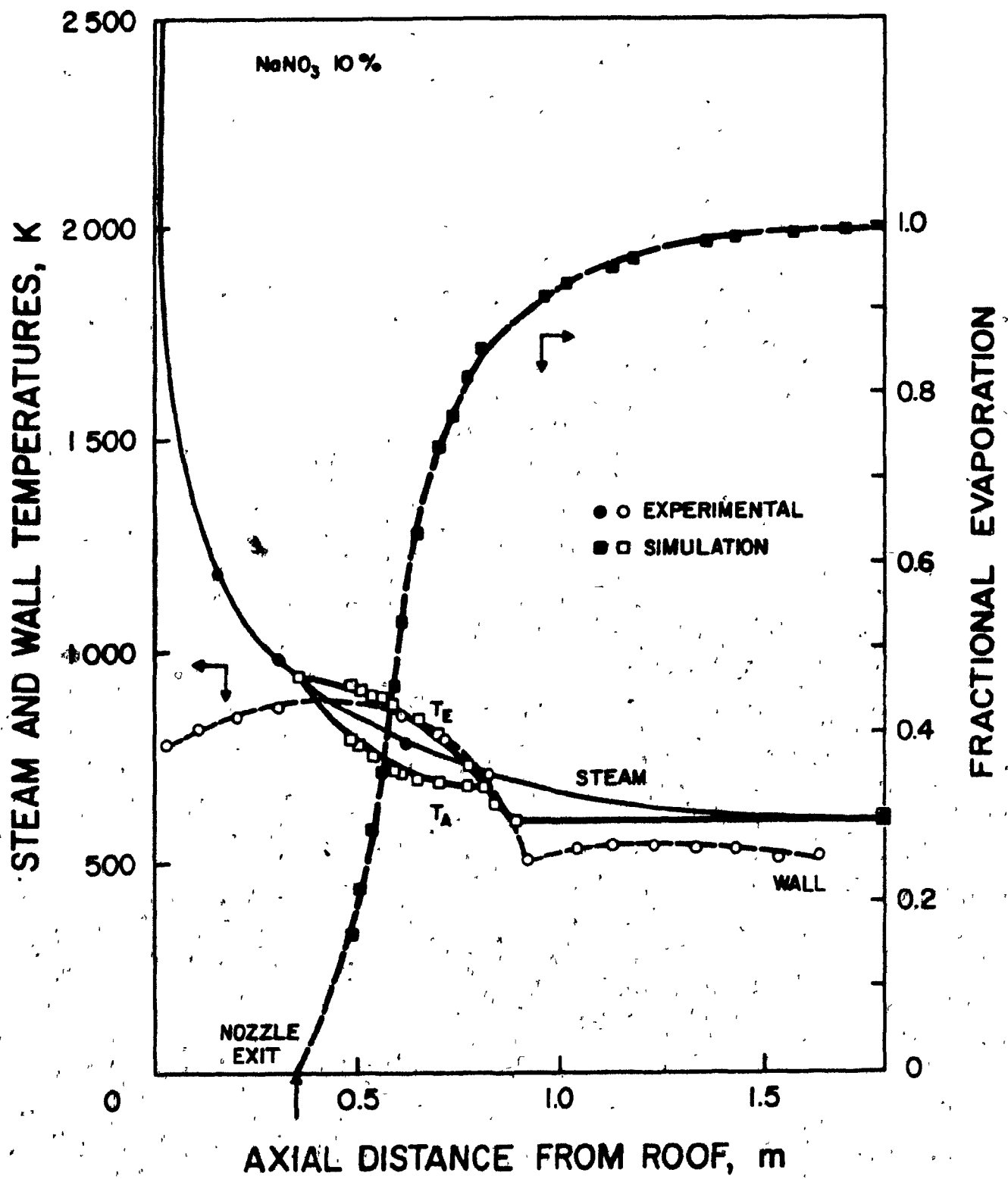


TABLE X

RESULTS FOR PURE WATER - NOZZLE MONARCH - RUN C

|                                                 | Simulation<br>Results | Experimental<br>Conditions              |
|-------------------------------------------------|-----------------------|-----------------------------------------|
| Radius of Cylindrical Chamber                   | 0.205 m               | 0.150 m                                 |
| Height of Cylindrical Chamber<br>Below Atomizer | 1.97 m                | 1.42 m                                  |
| Corresponding Residence<br>Time of Steam        | 4.09 s                |                                         |
| Outlet Temperature                              | 560. K                | 560 K                                   |
| Maximum Feed Flow Rate                          | 15.0 kg/h             | 15.0 kg/h                               |
| Maximum Rate of Water<br>Evaporation            | 14.70 kg/h            | 15.0 kg/h                               |
| Maximum Rate of Powder<br>Production            | 0.30 kg/h             | -                                       |
| Flow Rate of "Plasma Steam"                     | 1.44 kg/h             | 1.25 kg/h <sup>2</sup>                  |
| Heat Transferred to the<br>Feed Components      | 12.62 kW              | 12.7 kW<br>(12.55 + 0.15 <sup>1</sup> ) |
| Heat Losses Through the<br>Chamber Walls        | 0.38 kW               | 0.30 kW                                 |
| Effective Power Required<br>by Plasma           | 13.00 kW              | 13.0 kW                                 |
| Power Required by<br>Plasma, Gross              | 18.6 kW <sup>2</sup>  | 25.5 kW                                 |

<sup>1</sup> Heat transferred to low-temperature steam fed to plasma torch.

<sup>2</sup> Based on manufacturer's specification of 70% efficiency for their water vapor plasma torch.



FIGURE 9

PURE WATER - NOZZLE MONARCH

RUN C

EXPERIMENTAL AND CALCULATED

TEMPERATURE PROFILES

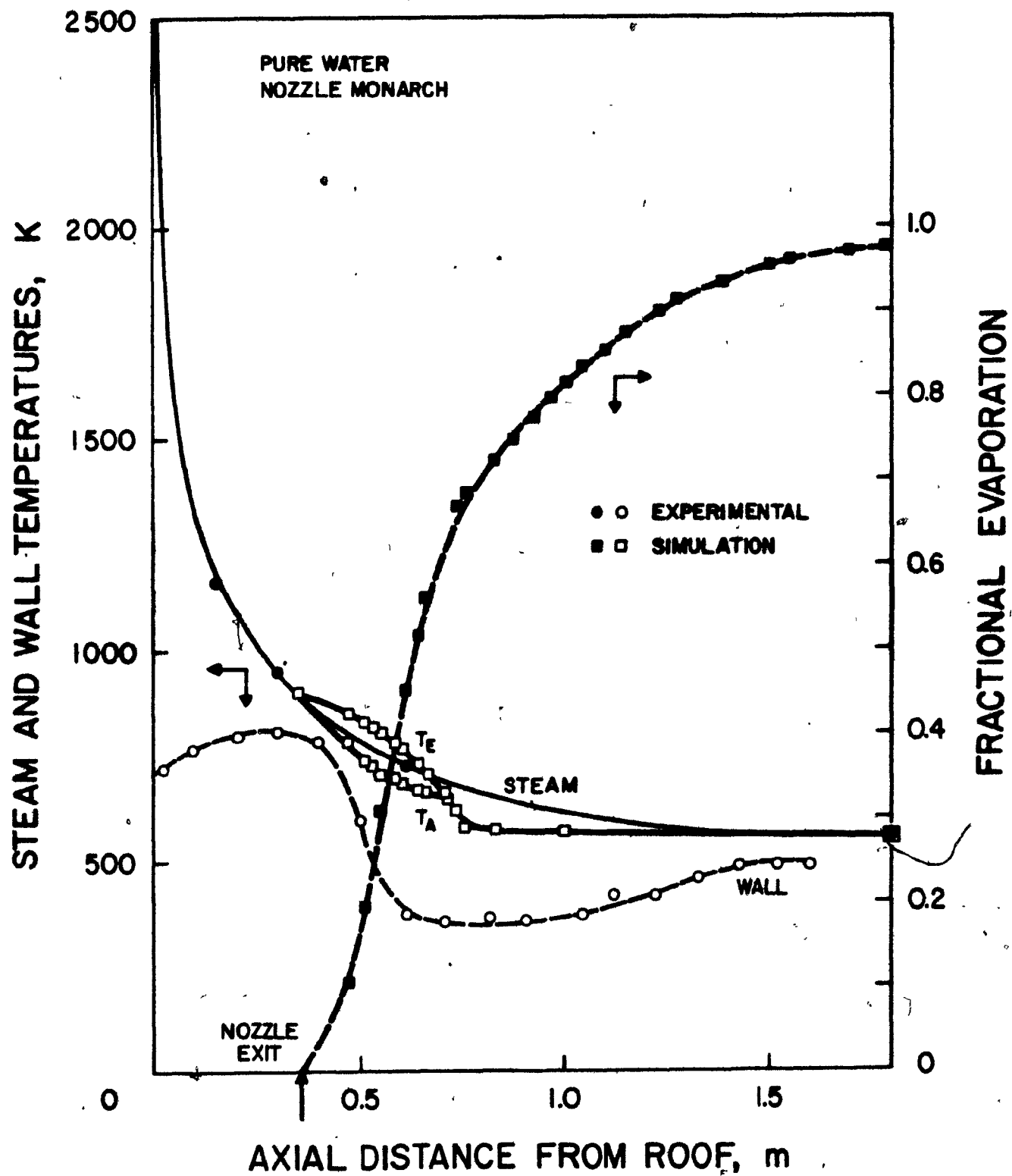


TABLE XI

RESULTS FOR PURE WATER - NOZZLE LNN2 - RUN D

|                                                 | Simulation<br>Results | Experimental<br>Conditions              |
|-------------------------------------------------|-----------------------|-----------------------------------------|
| Radius of Cylindrical Chamber                   | 0.428 m               | 0.150 m                                 |
| Height of Cylindrical Chamber<br>Below Atomizer | 1.37 m                | 1.42 m                                  |
| Corresponding Residence<br>Time of Steam        | 2.71 s                |                                         |
| Outlet Temperature                              | 560 K                 | 560 K                                   |
| Maximum Feed Flow Rate                          | 14.5 kg/h             | 14.5 kg/h                               |
| Maximum Rate of Water<br>Evaporation            | 14.2 kg/h             | 14.5 kg/h                               |
| Maximum Rate of Powder<br>Production            | 0.3 kg/h              | -                                       |
| Flow Rate of "Plasma Steam"                     | 1.40 kg/h             | 1.25 kg/h                               |
| Heat Transferred to the<br>Feed Components      | 12.23 kW              | 12.2 kW<br>(12.05 + 0.15 <sup>1</sup> ) |
| Heat Losses Through the<br>Chamber Walls        | 0.61 kW               | 0.6 kW                                  |
| Effective Power Required<br>by Plasma           | 12.84 kW              | 12.8 kW                                 |
| Power Required by<br>Plasma, Gross              | 18.3 kW <sup>2</sup>  | 24.8 kW                                 |

<sup>1</sup> Heat transferred to low-temperature steam fed to plasma torch.

<sup>2</sup> Based on manufacturer's specification of 70% efficiency for their water vapor plasma torch.

  
FIGURE 10

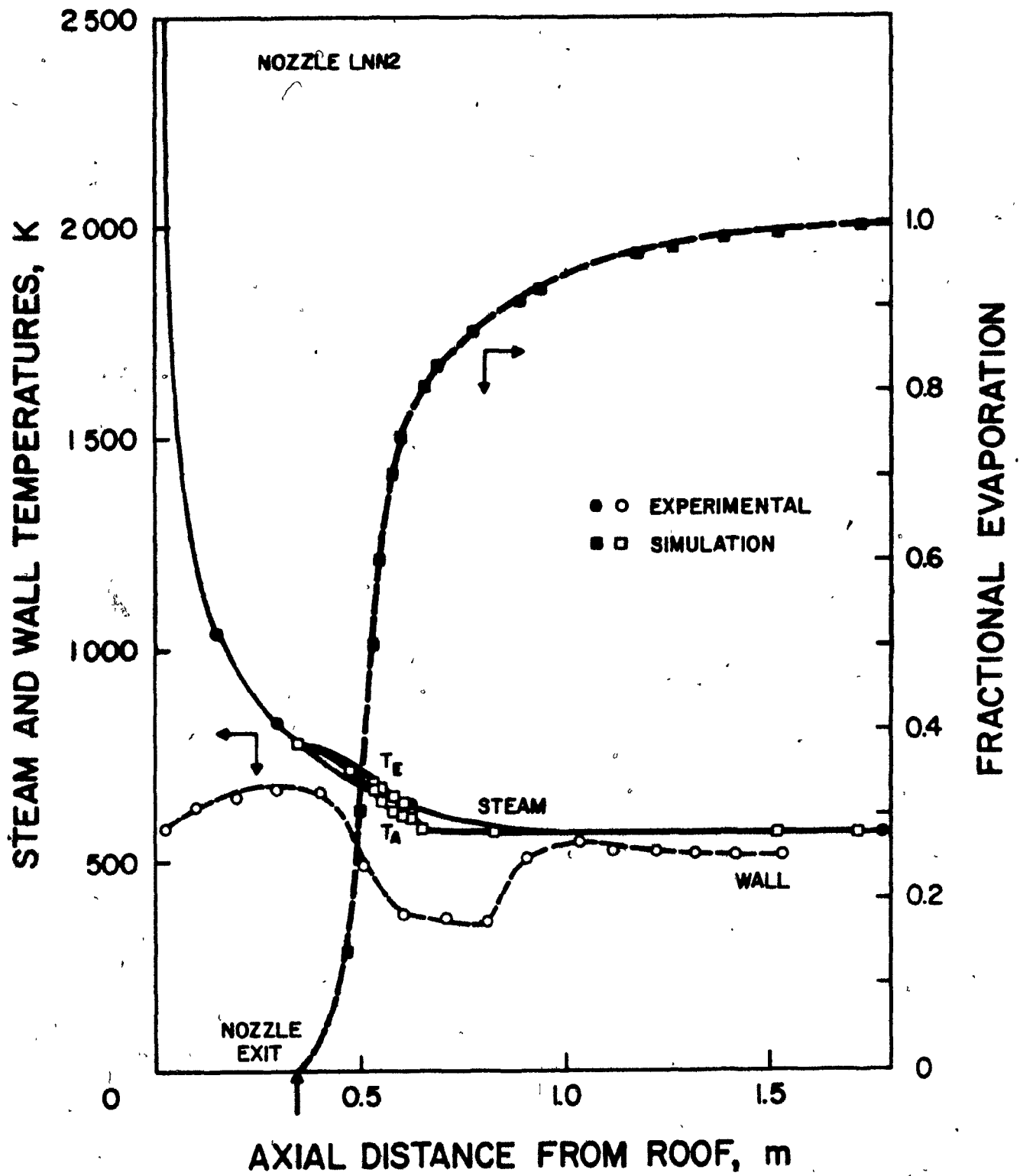
PURE WATER - NOZZLE LNN2

RUN D

EXPERIMENTAL AND CALCULATED

TEMPERATURE PROFILES





## DISCUSSION OF THE RESULTS OF THE SIMULATION

From the preceding tables, one can see that there is a fairly good agreement between the experimental results and those from the simulation, although a great number of assumptions have been made in the model.

As far as the heat transferred to the feed components is concerned (the term  $\Delta H$  defined in Equation (7) of Part I of the Experimental Section) the simulation gives exactly the true value. The experimental value actually consists of two terms: the small amount of energy used to heat the low-temperature steam injected into the plasma torch up to the outlet dryer temperature  $T_o$ , which is always very close to 0.15 kW, and the heat effectively used to dry the feed. In a real industrial operation where the steam to the torch accounts for the whole of the plasmagen gas, the first term drops off and the heat effectively used in the dryer is given directly in the simulation results. The value of the heat losses through the dryer walls which is experimentally defined as a given percentage of the heat effectively used in the dryer and experimentally determined by the overall heat balance on the chamber introduces then a slight difference (at the very most 3% of the calculated value) between the experimental and calculated values of the effective power delivered to the chamber.

The actual mass flow rate of steam injected in the experimental nitrogen plasma flame is 15% lower than the equivalent.

calculated mass flow rate of steam which would sustain a pure water vapor plasma jet of the same effective power; that proves indeed that nitrogen can be neglected with confidence when simulating the drying process, since it carries such a small fraction of the total energy.

The calculated temperature profiles show the same general trend in the four cases studied; at the beginning of the nozzle zone, the rate of evaporation is very high and remains almost constant, equal to its initial value; the initial decrease of the drying steam temperature  $T_A$  is therefore important, due to the drastic cooling effect of the spray and the first appearance of saturated steam generated at the boiling point temperature by the evaporation process; the surrounding steam is not so rapidly affected by the intense evaporation and its temperature decreases very smoothly because of the heat lost through the dryer walls and in the entrained steam. At the end of the nozzle zone, 80% to 90% of the total evaporation has already occurred and the rate of evaporation falls off very quickly to reach a very low value in the free-entrainment zone; the temperature  $T_A$  of the drying steam within the jet decreases very smoothly, whereas that of the surrounding steam,  $T_E$ , decreases more steeply towards  $T_A$  because of the intense mixing with the recirculating steam and with the increasing amount of low-temperature steam generated by the evaporation process. Finally, in the free-entrainment zone, the temperature of the steam is uniform throughout the whole section and almost constant, equal to the dryer outlet temperature  $T_O$ , because of the intense recirculation and turbulence; it results in a considerable

added length (and time) required to dry the largest droplets from 50% residual moisture content to a completely dry solid, at that relatively low temperature. It should be mentioned here that the criterion of operation which has been used in this simulation (that is, the production of a completely dry solid) is unrealistic from an industrial point of view, as it requires an inordinate amount of time to dry a few large droplets. However, the program is flexible enough so that it can be adapted to any product specifications or drying criterion that may be selected.

The simulation results clearly confirm the trend of the experimental findings concerning the evaporative capacity of the chamber for different concentrations of the feed solution. The small length of chamber needed to dry 18.2 kg/h of solution of 30%  $\text{NaNO}_3$  (40% unutilized) confirms indeed the fact that it certainly does not correspond to the chamber maximum capacity. For 10%  $\text{NaNO}_3$ , the simulated and experimental results are very close together, and much confidence is given to this set of values which were carefully established experimentally and which are remarkably fitted by the simulation; the corresponding maximum evaporative capacity is 14 kg/h of water.

The chamber operated with pure water and the same type of nozzle had a maximum capacity of 15 kg/h but this seems to be somewhat overestimated according to the simulation which gives a total length of the chamber 40% overestimated. On the other hand, the operation with pure water and the large cone-angle LNN2 nozzle is

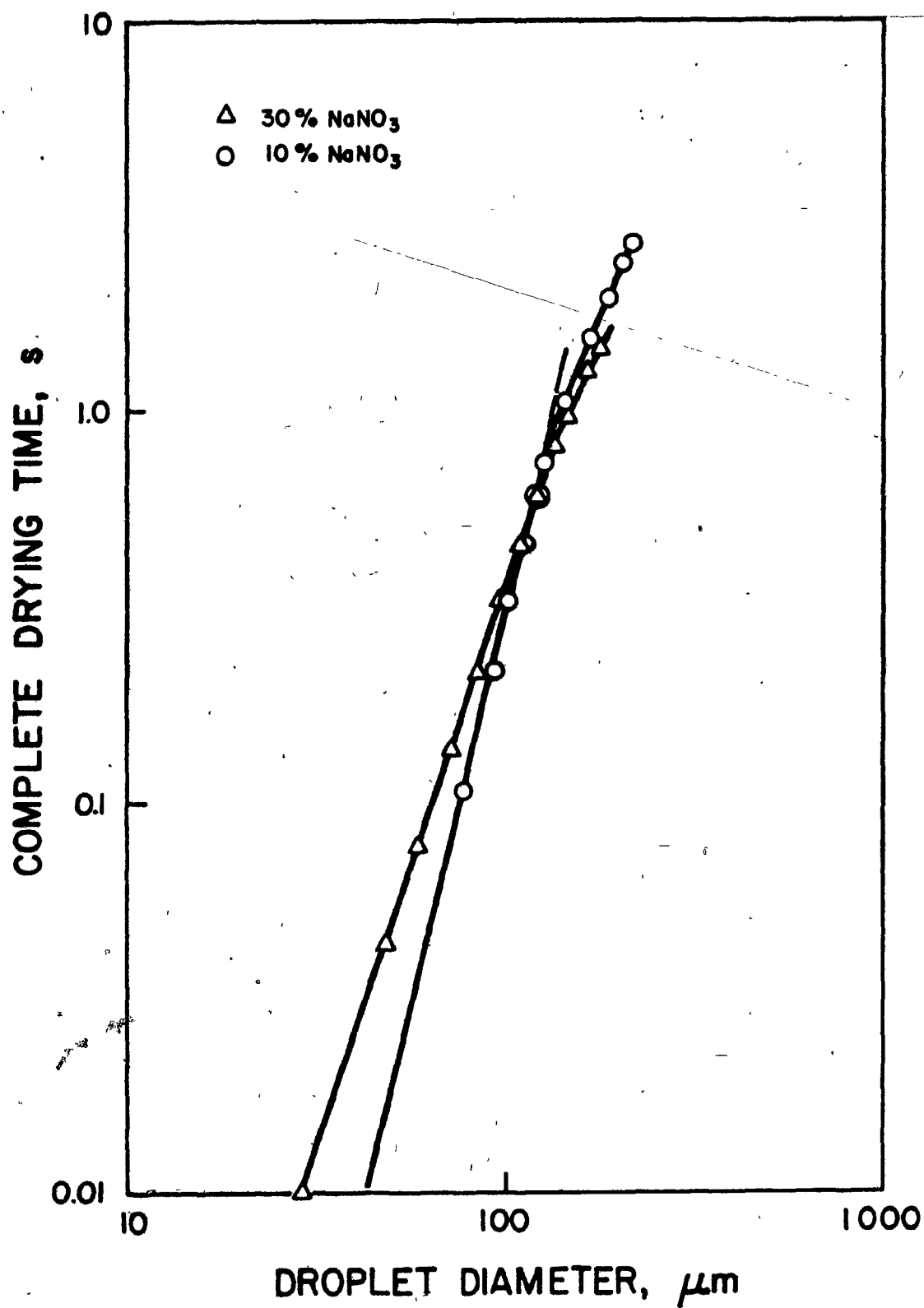


closely described by the model; the only really different variable is the calculated radius of the cylindrical chamber which is almost three times that of the experimental one; this underlines the importance of impingement of droplets on the walls with that nozzle. For the other runs (Tables VIII, IX, X), the radius of the cylindrical chamber is somewhat larger than the experimental radius, indicating that the impingement of the still wet largest droplets occurred at about 60 cm below the atomizer, whereas it happened at 18 cm below the atomizer with the LNN2 nozzle. Moreover, it is interesting to note that the radial velocity of the largest droplets at the periphery of the spray gets closer and closer to zero as the concentration of these droplets increases beyond 60%; thus, if a design radius is carefully chosen for the chamber according to that observation, no particle should ever hit the walls, no matter how long the dryer is. Some of the discrepancies between the experimental and the simulated results may also be attributed, to a certain extent, to the large uncertainty in the initial drop size distributions which were determined in a different ambient medium and which were therefore somewhat finer than the actual distributions generated during the experiments.

The extent of the drying period as a function of the initial droplet diameter is shown in Figure 11, on a log - log plot, for both solutions of 30% and 10%  $\text{NaNO}_3$ . An excellent straight line relationship is obtained for both concentrations. However, for droplets larger than 130  $\mu\text{m}$ , the slopes of the straight lines change

FIGURE 11

DRYING TIME AS A FUNCTION OF THE  
INITIAL DROPLET DIAMETER FOR SOLUTIONS  
OF 30%  $\text{NaNO}_3$  and 10%  $\text{NaNO}_3$



markedly, indicating a faster rate of drying for these larger drops, which may be due to a change in the drying mechanism. Incidentally, this change of slope of the line is not evident when the drying proceeds in relatively low-temperature air, as can be seen from an identical plot (Figure 12) shown in the paper by Gauvin and Katta (1976) for the simulation of a conventional hot-air spray drying system, using the same model.

### CONCLUSIONS

The development of the model which has just been presented and its comparison with the experimental results obtained in a pilot-plant installation have identified several weaknesses which must be corrected before an extrapolation to industrial capacity can be successfully undertaken. Several modifications and improvements were indeed obtained from the experimental evidence and have been incorporated in the first model developed by Gauvin in 1981, so that the present simulation is much improved and fits quite satisfactorily the experimental results (and particularly the steam temperature profile within the chamber). However, its further application to the design of a much larger industrial-capacity spray dryer has not been attempted because of the lack of adequate correlations, particularly for the entrainment of steam in the annulus surrounding the jet and its recirculation rate.

The following conclusions may now be offered as a result of the simulation study:

- ° First of all, the simulation confirms the general trend of the experimental findings concerning the evaporative capacity of the chamber for different feed concentrations, and gives particularly good agreement in the case of the 10%  $\text{NaNO}_3$  solution and of pure water atomized through the LNN2 nozzle.
- ° It shows that the most important parameter governing the evaporative capacity is the electrical power effectively supplied to the system and used for drying. The dimensions of the chamber are dictated to a certain extent by the atomization characteristics (spray angle, for example, which affects the diameter) while the length is also affected by the drop size distribution. Thus, the atomizer must be carefully chosen for a given application and product specifications.
- ° The major weakness of the simulation results from the complexity of the steam flow patterns induced in the chamber by the superposition of the plasma jet and of the atomized spray jet. The recirculation pattern assumed in the simulation could not be experimentally verified and neither its flow rate

nor its temperature profile (and hence its enthalpy) is known. The importance of measuring the radial temperature profiles at different axial positions within the chamber is evident. These profiles, had they been experimentally determined, would have indicated the recirculating steam temperature profile (and hence its flow rate from heat balances) as well as the end of the nozzle zone.

Summarizing, there is a real need to develop adequate semi-empirical correlations for:

1. The entrainment of superheated steam within a liquid spray.
2. The recirculation of surrounding steam which is induced by the pumping effect of the spray jet and of the plasma jet confined in a relatively narrow chamber, related to the geometry and the dimensions of the chamber.
3. The steam axial velocity in the whole chamber and particularly in the free-entrainment zone.
4. The steam flow patterns in the mixing zone above the atomizing nozzle.

In spite of the preliminary nature of the results which have been presented, it is felt that they are sufficiently promising to justify further work with this novel approach.

### NOMENCLATURE

- $Bi$  - Biot number, dimensionless
- $C$  - droplet solid concentration (%)
- $C_{LD}$  - instantaneous solid concentration of the largest droplet (%)
- $C_p$  - heat capacity (J/kg K)
- $D_n$  - diameter of nozzle (m)
- $D_{VS}$  - Sauter mean diameter of the spray ( $\mu m$ )
- $E, E_x$  - rate of evaporation (kg/s)
- $E_p$  - entrainment parameter, defined in Equation (14)
- $H_E$  - enthalpy of entrained steam evaluated at  $T_E$  (J/kg)
- $H_F$  - enthalpy of feed solution at  $T_F$  (J/kg)
- $\Delta H_F$  - energy required to heat one kg of the feed solution from  $T_F$  up to its boiling point (J/kg)

- $H_G$  - enthalpy of plasma steam
- $H_N$  - enthalpy of steam at the nozzle level (J/kg)
- $H_O$  - enthalpy of steam at exit (J/kg)
- $H_{P_O}$  - enthalpy of solid product at exit (J/kg)
- $H_T$  - enthalpy of recirculating steam at  $\underline{T_T}$  (J/kg)
- $H_{T_A}$  - enthalpy of steam at  $\underline{T_A}$  (J/kg)
- $H_{T_E}$  - enthalpy of steam at  $\underline{T_E}$  (J/kg)
- $H_{T_W}$  - enthalpy of steam at its boiling point (J/kg)
- $k$  - thermal conductivity of superheated steam  
at  $\underline{T}$  Equation (5) (W/mK)
- $k_d$  - thermal conductivity of the droplet  
solution (W/mK)
- $k_o$  - thermal conductivity of steam at zero pressure  
Equation (6) (W/mK)
- $K_v$  - velocity coefficient defined by Equation (22)
- $Nu$  - Nusselt number, dimensionless
- $P$  - atomizing pressure (N/m<sup>2</sup>)
- $q_L, q_{Lx}$  - heat losses through the dryer walls (J/s)



- $r$  - radial distance from the center of the chamber (m)
- $r_5$  - velocity half radius, Equation (20) (m)
- $R$  - radius of chamber (m)
- $T$  - temperature (K)
- $T_A$  - temperature of drying steam within the jet (K)
- $T_E$  - temperature of entrained surrounding steam in the annulus of the jet; also temperature of drying steam in free entrainment zone (K)
- $T_F$  - temperature of feed solution (K)
- $T_O$  - temperature at outlet of dryer (K)
- $T_T$  - temperature of steam recirculated (K)
- $T_W$  - boiling point of solution (K)
- $V_c$  - steam axial velocity on the centerline of the spray (m/s)
- $V_n$  - average velocity of the liquid leaving the atomizing pressure nozzle, Equation (13) (m/s)
- $V_v$  - axial velocity of steam within the chamber (m/s)

- $W$  - generally, mass flow rate (kg/s)
- $W_E$  - mass flow rate of steam entrainment within the jet Equation (13) (kg/s)
- $W_F$  - mass flow rate of feed (kg/s)
- $W_G$  - "equivalent" mass flow rate of plasma (kg/s)
- $W_o$  - mass flow rate of steam at exit of the dryer (kg/s)
- $W_p, W_{p_x}$  - mass flow rate of solid product (kg/s)
- $W_T$  - instantaneous mass flow rate of steam recirculated (kg/s)
- $W_{T_i}$  - initial value of mass flow rate of steam recirculated at nozzle level (kg/s)
- $W_{T_f}$  - final value of mass flow rate of steam recirculated at any point within the free entrainment zone (kg/s)
- $W_W$  - chamber evaporating capacity (kg water evaporated/s)
- $x$  - axial distance from nozzle (m)
- $x'$  - axial distance at which the corresponding  $W_E$  is zero, Equation (13) (m)
- $x''$  - maximum distance beyond which  $W_E$  does not vary linearly with  $x$ , (m)

Greek Symbols

- $\lambda$  - latent heat of vaporization (J/kg)
- $\rho$  - density (kg/m<sup>3</sup>)
- $\mu$  - viscosity (kg/ms)
- $\eta_t$  - torch thermal efficiency (%)
- $\theta$  - angle of spray with centerline (radians)

Subscripts

- $i$  - initial value
- $f$  - final value
- $x$  - value of the variable at a distance  $x$   
from the nozzle
- $L$  - liquid
- $G$  - drying steam
- $S$  - solid

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### SUMMARY AND CONTRIBUTION TO KNOWLEDGE

Solutions of different concentrations (10% and 30%) of sodium nitrate and pure water were spray-dried in a heavily insulated cylindrical drying chamber, using a plasma of nitrogen and superheated water vapor (50% by mass) as a direct and efficient heat source for the operation. These experiments were followed by the development of a computer simulation to facilitate the interpretation of the experimental results and the understanding of the phenomena involved. It confirmed the general trends of the experimental results and the following conclusions may be shown from both studies:

1. The new drying process is quite feasible from a technical point of view; however, it is applicable at the present time especially to non-heat-sensitive materials of high melting point and high decomposition temperature because of the possibility of product degradation due to turbulence and recirculation phenomena. However, heat-sensitive materials might be successfully dried in that way in small chambers specially designed to minimize or suppress recirculation and turbulence.

2. For a given feed concentration and a given power to the plasma jet, an optimum range of feed rates can be found, corresponding

to an optimum drying efficiency. To the left of that optimum, the product quality is rapidly lost by over-heating since the loading is too low to use all the energy of the steam for the drying process, and to its right, the loading is too high to be completely dried,

3. A very small amount of plasma-generated high temperature dissociated water vapor is required to perform the whole drying process: it amounts only to about 10% by mass of the maximum evaporative capacity of the drying chamber.

4. Provided the energy contained in the steam at the outlet of the dryer can be completely recovered, the efficiency of the spray drying operation itself is about 80% (it does not include the torch efficiency), which is much higher than that of the majority of conventional hot-air spray dryers (about 60%). It supposes then that a sophisticated product collection system introducing no drastic heat losses and pressure drop, and an efficient condensation unit may be provided at the exit of the dryer.

5. The small dimension of the chamber and its correspondingly high evaporative capacity ( $24.5 \text{ kg/(h)(m}^3\text{)}$  of chamber compared to  $5 \text{ kg/(h)(m}^3\text{)}$  for conventional systems) are very promising from an economic point of view. Most important is the considerable saving in energy resulting from the recovery of the heat in the exhaust steam from the system; these economic predictions now based on experimental evidence should promote the further development of the experimental program.



6. In spite of the great number of assumptions made in the course of the model development, particularly for the entrainment and recirculation rates, the simulation fits the experimental results remarkably well and confirms the general conclusions obtained from them.

7. Qualitative observations were made on the mode of crystallization of sodium nitrate; small droplets are much influenced by high temperature and may burst not far from the atomizer; one or more cubical crystals grow in the largest droplets, whereas a simultaneous formation of a rigid porous crust may occur at the surface of several of them.

#### SUGGESTIONS FOR FURTHER WORK

It is apparent from the results of this thesis that further experimental work is required so that a better understanding of the phenomena involved in the process may be obtained. The design of systems of industrial capacity will not be possible until the following investigations are performed:

1. The entrainment of superheated steam within a liquid spray should be correlated as a function of the atomization characteristics, the feed properties and the chamber dimensions.
2. The recirculation of surrounding steam induced by the pumping effect of an atomized jet and of a plasma jet confined in

a relatively narrow chamber should be correlated in relation to the geometry and dimensions of the chamber.

3. The turbulence and recirculation phenomena should be minimized in the chamber to avoid over-heating of the entrained product particles, but it should not add any heat losses to the system, since the thermal efficiency is of prime importance.

4. The steam and droplets axial velocities should be correlated in the whole chamber and particularly in the free-entrainment zone.

5. The steam flow patterns in the mixing zone should be described in relation with the plasma jet characteristics and the chamber geometry and dimensions.

6. Different non-heat-sensitive materials (such as KCl) and heat-sensitive materials too (such as milk) should be tested under various drying conditions and in different chamber designs.

7. The fundamental mechanisms involved in the process of drying finely divided droplets of aqueous solutions in superheated steam should be elucidated.

APPENDIX A

TORCH CHARACTERISTICS

## APPENDIX A

### TORCH CHARACTERISTICS

Table AI shows a set of typical operating parameters which were recorded during the experimental runs performed on pure water with a Monarch nozzle.

The first two columns are the arc current and the true arc voltage between the electrodes; the torch cooling water flow rate and temperatures are then given; the last four columns are successively the energy fed to the torch ( $P_{in}$ ), the heat losses to the electrodes ( $P_{lost}$ ), the energy effectively transferred to the plasma jet ( $P_{out}$ ) and the torch efficiency. They were calculated in SI units by the following equations:

$$P_{in} = U \times I \quad (A1)$$

$$P_{lost} = Q_c (H_{c out} - H_{c in}) \quad (A2)$$

and

$$P_{out} = P_{in} - P_{lost} \quad (A3)$$

The torch efficiency (in %) was defined by the following ratio:

$$\text{Efficiency } \eta_T = P_{out} / P_{in} \quad (A4)$$

TABLE AI

EXPERIMENTAL OPERATING PARAMETERS

| Run No | I (A) | U (V) | Q <sub>c</sub> (kg/h) | T <sub>c in</sub> (°F) | T <sub>c out</sub> (°F) | P <sub>in</sub> (kW) | P <sub>lost</sub> (kW) | P <sub>out</sub> (kW) | Torch Efficiency (%) |
|--------|-------|-------|-----------------------|------------------------|-------------------------|----------------------|------------------------|-----------------------|----------------------|
| F1     | 390   | 66    | 688.6                 | 37                     | 65.5                    | 25.7                 | 12.7                   | 13.0                  | 50.7                 |
| F2     | 400   | 64    | 688.6                 | 37                     | 65.5                    | 25.6                 | 12.7                   | 12.9                  | 50.4                 |
| F3     | 400   | 64    | 688.6                 | 37                     | 65.5                    | 25.6                 | 12.7                   | 12.9                  | 50.4                 |
| F4     | 395   | 64.5  | 688.6                 | 37                     | 65.5                    | 25.5                 | 12.7                   | 12.8                  | 50.2                 |
| G1     | 395   | 64.5  | 688.9                 | 38                     | 66.5                    | 25.5                 | 12.7                   | 12.8                  | 50.2                 |
| G2     | 395   | 65    | 688.9                 | 38                     | 67                      | 25.7                 | 12.9                   | 12.8                  | 49.8                 |
| G3     | 390   | 65.5  | 688.9                 | 38                     | 67                      | 25.55                | 12.9                   | 12.65                 | 49.5                 |
| G4     | 395   | 64.5  | 688.9                 | 38                     | 66                      | 25.5                 | 12.5                   | 13.0                  | 51.0                 |

It is clear from that table that the energy effectively transferred to the plasma jet was relatively steady throughout a run, despite the variation of the couple (amperage, voltage) characteristic of the arc.

An estimation of the systematic error introduced when reading the different operating variables,  $I$ ,  $U$ ,  $Q_c$ ,  $T_c$  is also very interesting since it determines the precision of the most important operating parameter,  $P_{out}$ ; let us assume the torch operating variables were read with the following precisions:

$$\Delta I = 5 \text{ A}$$

$$\Delta U = 0.5 \text{ V}$$

$$\Delta Q_c = 1 \text{ lb/min} = 2.1 \cdot 10^{-6} \text{ kg/s}$$

$$\Delta T_c = 1^\circ \text{F which gives } \Delta = 0.5 \text{ Btu/lb} = 1.16 \cdot 10^3 \text{ J/kg}$$

$$\text{as } P_{out} = UI - Q_c (H_{c \text{ out}} - H_{c \text{ in}}) \quad (\text{A2})$$

$$\Delta P_{out} = U \Delta I + I \Delta U + (\Delta Q_c) (H_{c \text{ out}} - H_{c \text{ in}}) + Q_c [\Delta H_{c \text{ out}} - \Delta H_{c \text{ in}}] \quad (\text{A5})$$

Replacing in (A5) for the typical run A2:

$$\Delta P_{out} = 0.960 \text{ kw}$$

or  $\Delta P_{out} / P_{out} = 7.5\%$  which is the relative error made in the determination of the net power of the jet.

From (A1),

$$\Delta P_{in} = \dot{M}\Delta T + I\Delta U \quad (A6)$$

$\Delta P_{in} = 0.520 \text{ kW}$  which makes a relative error of 2% on  $P_{in}$ ,

and finally

$$\Delta \text{Efficiency} / \text{Efficiency} = \Delta P_{out} / P_{out} - \Delta P_{in} / P_{in} \quad (A7)$$

The relative error on the determination of the torch efficiency in the run A2 is then only 5.5% whereas it is 7.5% on the net power of the plasma jet.

Identical calculations, which did not give much different results, were done on torch parameters whenever it was needed.

From a knowledge of the net power transferred to the plasma gas mixture and of the composition of that mixture, the specific enthalpy of the mixture and the mean temperature and velocity of the jet at the exit of the anode-nozzle can be computed.

Effective power into the plasma gas mixture  $P_{out}^* = 12.9 \text{ kW}$ .

Composition of the gas mixture:

1.25 kg/h steam which gives  $x_s = 0.440$

1.59 kg/h  $N_2$   $x_{N_2} = 0.560$

The specific enthalpy of the mixture at the nozzle exit is:

$$\underline{H_m = 1.64 \cdot 10^7 \text{ J/kg} = 3907 \text{ kcal/kg}} \quad (\text{A8})$$

Assuming the plasma gas mixture is an ideal mixture of ideal gases:

$$H_m = x_s H_s + x_{N_2} H_{N_2} \quad (\text{A9})$$

In order to find the temperature of the mixture, its specific enthalpy is computed according to (A9) at different temperatures and the result is compared to (A8).

At  $T = 3\,580 \text{ K}$ :

The enthalpy of steam is calculated by interpolating between  $3\,500 \text{ K}$  and  $3\,600 \text{ K}$  in the values presented by Ihara (1977); at  $3\,500 \text{ K}$ ,  $H_s$  is given by the following equation, the reference temperature being  $1\,000 \text{ K}$ :

$$H_s = 952.4 + \left[ \frac{33.317}{11.103} + \frac{51.602}{18.016} \right] 10^3 \quad (\text{A10})$$

$$= 6817.4 \text{ kcal/kg}$$

and at  $3\,580 \text{ K}$ ,

$$H_s = 6817.4 + \left[ \frac{40.900}{10.206} - \frac{33.317}{11.103} \right] \left( \frac{3580 - 3500}{100} \right) 10^3$$

$$\underline{H_s = 7622.8 \text{ kcal/kg}}$$

for nitrogen, from Figures 10 and 11 in Sewell's and Fenter's report



(1962), one sees that no significant dissociation occurs below 8 000 K and then using Figure 19-c in the same report:

$$\underline{H_{N_2} = 964 \text{ kcal/kg}}$$

then  $H_m = 3894 \text{ kcal/kg}$  which is really quite close to the experimental value (error < 0.5%).

Let us determine now the molecular weight of the mixture at the exit of the nozzle:

from Ihara's tables,  $M_s = 9.960 \text{ g}$  (by interpolation)

from Sewell's and Fenter's report, for non-dissociated nitrogen,

$$\dot{M}_{N_2} = 28.01 \text{ g} \quad (\text{from their Figure 19-c})$$

$$M_m = y_s M_s + y_{N_2} M_{N_2}$$

with  $y_s = 0.688$ ,  $y_{N_2} = 0.312$  (computed from the mass percentages  $x$ )

$$\underline{M_m = 15.6 \text{ g/mole}}$$

#### Velocity of the Jet at Nozzle Exit

The volumetric gas flow rate in the nozzle is, according to the ideal gas law:

$$PV = \dot{m} RT / M_m \quad (\text{A11})$$

or

$$\dot{V} = \dot{m} RT/P M_m$$

$$\dot{V} = \frac{(1.59 + 1.25) \times 8.314 \times 3580}{3600 \times 1.013 \times 10^5 \times 15.6 \times 10^{-3}}$$

$$\dot{V} = 1.49 \times 10^{-2} \text{ m}^3/\text{s}$$

Since the nozzle diameter is 0.794 cm (5/16 in), the gas velocity is:

$$V = \dot{V}/A = 4 \dot{V} / \pi \times (0.794 \times 10^{-2})^2$$

$$V = 300 \text{ m/s}$$

#### REFERENCES

Ihara, S., "Approximations of the Thermodynamic Properties of High Temperature Dissociated Water Vapor," Bulletin Electrotechnical Laboratory, Tokyo, p 13-34 (Denki Shikenjo Iho) February 1977

Sewell, K.G. and Fenter, F.W., "Thermodynamic Properties of High-Temperature Nitrogen," LTV Research Center, Ling - Temco - Vought Inc., Report No O.71000/2R-25, July 1962

NOMENCLATURE

- $A$  - Nozzle cross-sectional area ( $m^2$ )  
 $H_x$  - Specific enthalpy of  $x$  at  $T$  (J/kg)  
 $H_{c \text{ in}}$  - Specific enthalpy of cooling water at the entrance of the torch (J/kg)  
 $H_{c \text{ out}}$  - Specific enthalpy of cooling water at the exit of the torch (J/kg)  
 $I$  - Arc current intensity (A)  
 $M_x$  - Molecular weight of  $x$  (g/mole)  
 $\dot{m}$  - Total mass flow rate of plasma gas mixture (kg/s)  
 $P$  - Pressure of gas mixture ( $N/m^2$ )  
 $P_{in}$  - Fed to the torch (kw)  
 $P_{lost}$  - Power lost to the electrodes (kw)  
 $P_{out}$  - Power effectively transferred to the plasma gas (kw)  
 $Q_c$  - Mass flow rate of cooling water through the torch (kg/h)  
 $R$  - Ideal gas constant (J/mole K)

$T_{c \text{ in}}$  - Temperature of cooling water at the entrance of the torch ( $^{\circ}\text{C}$ )

$T_{c \text{ out}}$  - Temperature of cooling water at the exit of the torch ( $^{\circ}\text{C}$ )

$U$  - True arc voltage (Volts)

$V$  - Mean gas velocity at nozzle exit (m/s)

$\dot{V}$  - Total volumetric gas flow rate in nozzle ( $\text{m}^3/\text{s}$ )

$x_x$  - Mass fraction of  $x$

$y_x$  - Molecular fraction of  $x$

$\eta_T$  - Torch efficiency (%)

#### S u b s c r i p t s

$m$  - Gas mixture

$N_2$  - Nitrogen

$s$  - Steam

APPENDIX B

SAMPLE CALCULATION OF DRYING EFFICIENCY

## APPENDIX B

### SAMPLE CALCULATION OF DRYING EFFICIENCY

A sample calculation follows of the energy gained by the components of the feed (that is, the particles and then moisture) in the chamber, and of the drying efficiency for Run B2 from Equations (8), (9) of Part I. Let us calculate first some characteristic enthalpies required in all the calculations:

1. Specific Enthalpy  $H_F$  of the Feed Solution at

Concentration  $c_1$  and Temperature  $T_F = 10^\circ\text{C}$ :

$$H_F = c_1 H_{(\text{salt}, T_F)} + (1 - c_1) H_{(\text{water}, T_F)} + c_1 \Delta H_s \quad (\text{B1})$$

°  $\Delta H_s$  is the specific heat of solution  $\text{NaNO}_3$ ;

it is given in Perry's Handbook (page 3-149, 5th. Ed., (1975), by:

$$\Delta H_s = + 5050 \text{ cal/mole} = + 59.405 \text{ kcal/kg},$$

$$= + 248.6 \text{ kJ/kg}$$

(endothermic dissolution)

$$H_{(\text{water}, T_F)} = 10.03 \text{ kcal/kg} = 42.0 \text{ kJ/kg}$$

$$H_{(\text{salt}, T_F)}$$

The heat capacity of  $\text{NaNO}_3$  is given in Perry's  
(p 3-134) in cal/k mole: (reference:  $0^\circ\text{C}$ )

$$c_p = 4.56 + 0.0580 T_{(\text{K})} \quad \text{for } 273 \text{ K} < T < 583 \text{ K}$$

(solid crystal)

and  $c_p = 37.2$  for  $583 \text{ K} < T < 703 \text{ K}$

(liquid melted  $\text{NaNO}_3$ )

then

$$H_{(\text{salt}, T_F)} = \frac{1}{85.01 \cdot 10^{-3}} \int_{273}^{283} c_p dT$$

$$= 2.433 \text{ kcal/kg} = 10.18 \text{ kJ/kg}$$

We end up with

$$\underline{H_F = 216.8 c_1 + 42 \text{ kJ/kg.}}$$

## 2. Specific Enthalpy of Superheated Steam at $106^\circ\text{C}$

$$H_{(\text{steam}, 106^\circ\text{C})} = 642.2 \text{ kcal/kg} = 2688 \text{ kJ/kg}$$

The other characteristic enthalpies are the enthalpy of superheated steam and that of solid  $\text{NaNO}_3$  at the outlet temperature of the dryer, which are different in each run.

In the present case, the outlet temperature is 315°C or 588 K; the outlet mixture is completely dried and we assume that the salt has not enough time to melt, although it is at its melting point temperature 583 K. Its specific enthalpy at 583 K is:

$$H_{(\text{salt}, 310^\circ\text{C})} = \frac{1}{85.01 \cdot 10^{-3}} \int_{273}^{583} c_p dT = 107.2 \text{ kcal/kg}$$

$$= 448.5 \text{ kJ/kg}$$

The specific enthalpy of steam at 588 K is :

$$H_{(\text{steam}, 315^\circ\text{C})} = 741.8 \text{ kcal/kg} = 3105 \text{ kJ/kg}$$

We can then make the following calculations:

At the inlet of the chamber:

18.2 kg/h of solution at  $H_F = 107.1 \text{ kJ/kg} \rightarrow 1949.2 \text{ kJ/h}$

1.25 kg/h of superheated steam at  $H = 2688 \text{ kJ/kg} \rightarrow 3360 \text{ kJ/h}$

At the outlet of the chamber:

5.46 kg/h of dry  $\text{NaNO}_3$  at  $H = 448.5 \text{ kJ/kg} \rightarrow 2449 \text{ kJ/h}$

(12.74 + 1.25) kg/h of steam at  $H = 3105 \text{ kJ/kg} \rightarrow 43439 \text{ kJ/h}$

The total energy  $Q$  used to dry and to heat up the whole steam and solid product to the outlet temperature given by Equation (6) of Part I is equal to:



$$Q = \frac{(43439 + 2449) + (3360 + 1949.2)}{3600} = 11.3 \text{ kW}$$

The energy gained by the particles and their moisture only is equal to:

$$\frac{(3105 \times 12.74 + 2449) - 1949.2}{3600} = 11.1 \text{ kW}$$

By difference, we end up with the fraction of energy given to the low-temperature steam injected into the  $N_2$  plasma flame, which is almost a constant from one run to another, equal to 0.2 kW and in any case, it is less than 2% of the total energy  $P_{out}$  passed through the dryer.

The drying efficiency of the actual operation is, according to Equation (8) of Part I:

$$\eta_1 = \frac{13.99 [3105 - 83.8]}{(12.65 + 0.7) \times 3600 + 1949 + 3360} = 79\%$$

83.8 kJ/kg is the specific enthalpy of liquid water at 20°C, as it was assumed that steam was condensed to water at 20°C at the exit of the dryer.

With a true water vapour plasma under the same conditions,  $\eta_2$ , the drying efficiency, is given by Equation (9):

$$\eta_2 = \frac{12.74 [3105 - 83.8]}{(12.65 + 0.7) \times 3600 + 1949} = 77\%$$

The overall efficiency of the operation is calculated from Equation (8) with  $P_{in}$  instead of  $P_{out}$ :

$$\eta = \frac{13.99 [3105 - 83.8]}{(24.7 + 0.7) \times 3600 + 1949 + 3360} = 44\%$$

Another interesting result is the fraction of heat lost through the dryer walls; it is obtained by difference with the energy  $P_{out}$  introduced in the chamber:

$$Q_L = 12.65 - 11.3 = 1.35 \text{ kW}$$

It is expressed as a fraction of the total energy  $Q$  used in the chamber by:

$$Q_L = \frac{1.35}{11.3} \times Q = 12\% Q$$

APPENDIX C

PHOTOGRAPHS OF PRODUCT SAMPLES FROM  
THE BOTTOM OF THE CHAMBER

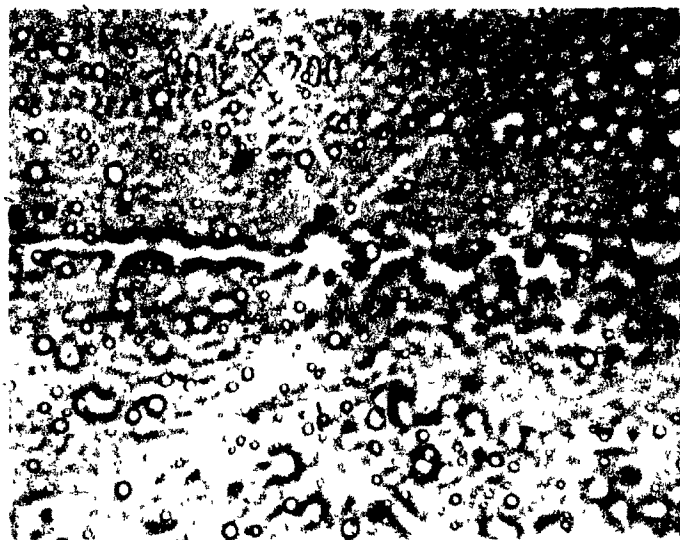


Figure C1. Pure Water - Nozzle LNN2 -  
Run H2 - 16 kg/h Feed - (100x)



Figure C2. Pure Water - Nozzle LNN2 -  
Run I1 - 14 kg/h Feed - (100x)

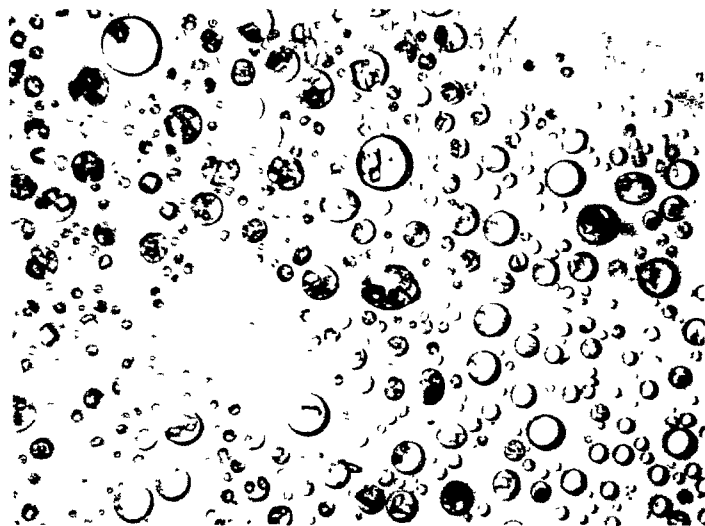


Figure C3. 10%  $\text{NaNO}_3$  - Run C1 -  
23 kg/h Feed - (100x)

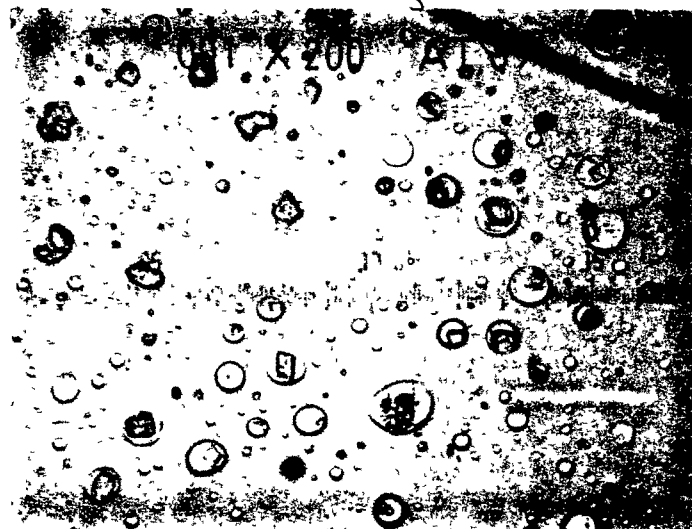


Figure C4. 10%  $\text{NaNO}_3$  - Run C2  
20 kg/h Feed - (100x)

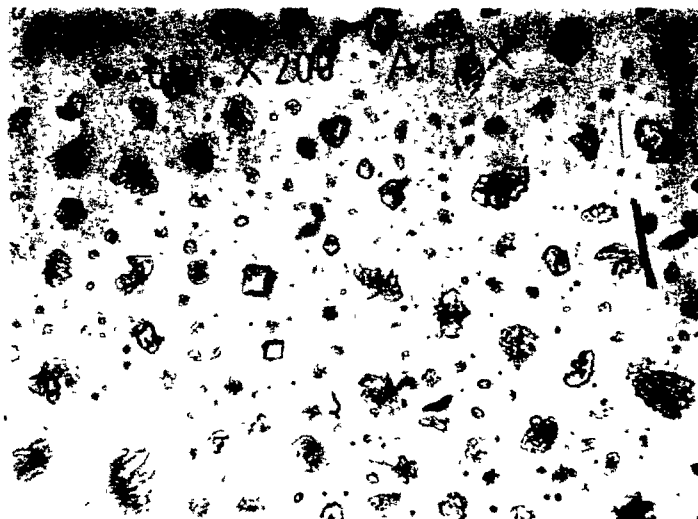


Figure C5. 10%  $\text{NaNO}_3$  - Run E4  
15.5 kg/h Feed - (100x)



Figure C6. 10%  $\text{NaNO}_3$  - Run E4  
15.5 kg/h Feed - (100x)

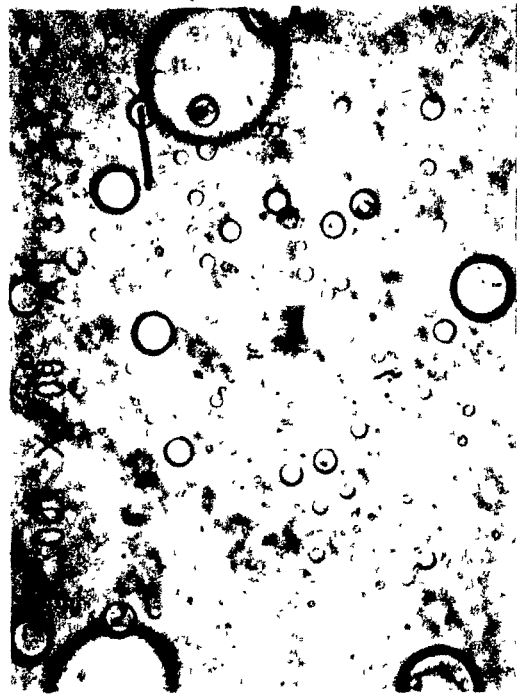


Figure C7. 30%  $\text{NaNO}_3$  - Run A1  
25.5 kg/h Feed - (100x)

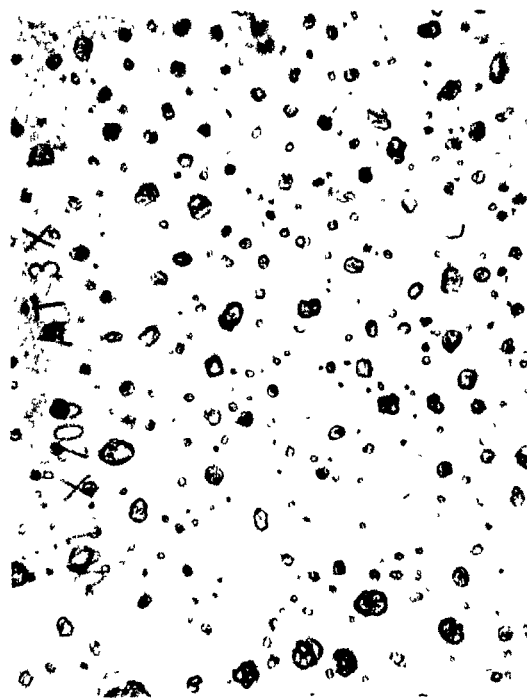


Figure C8. 30%  $\text{NaNO}_3$  - Run B1  
18.2 kg/h Feed - (100x)

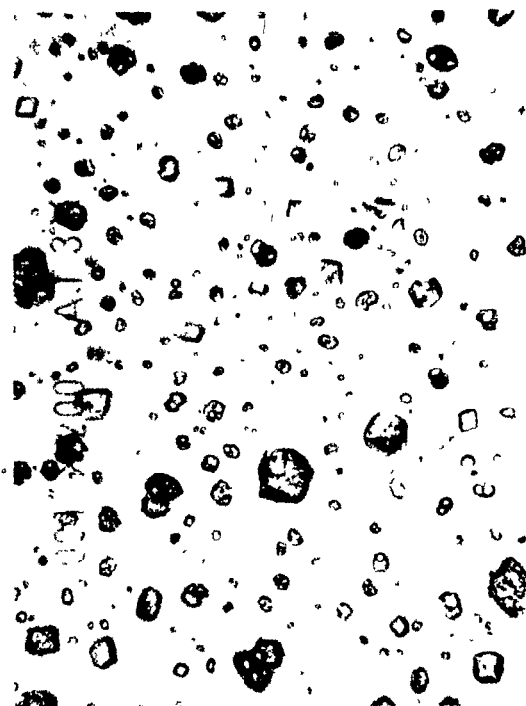


Figure C9. 30%  $\text{NaNO}_3$  - Run B1  
18.2 kg/h Feed - (100x)

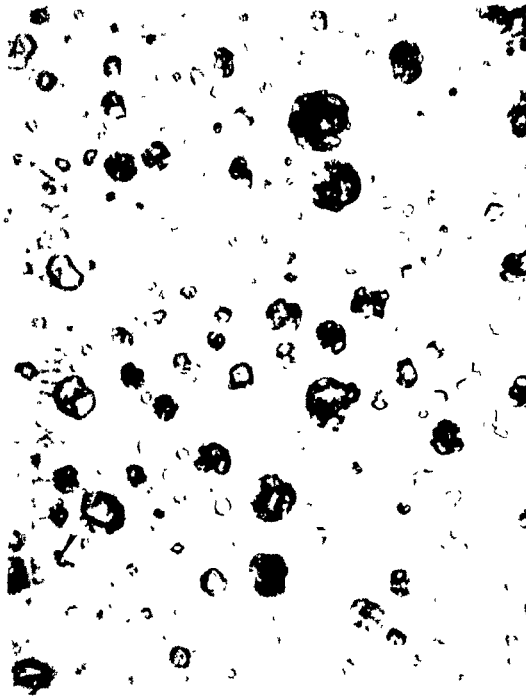


Figure C10. 30%  $\text{NaNO}_3$  Run B1  
18.2 kg/h Feed - (100x)

APPENDIX D

DETERMINATION OF THE SAUTER MEAN DIAMETER  $D_{VS}$

AND THE MAXIMUM STABLE DROPLET DIAMETER  $D_{MAX}$

ACCORDING TO MUGELE AND EVANS UPPER-LIMIT

FUNCTION IN THE FOUR CASES STUDIED

## APPENDIX D

### DETERMINATION OF $D_{VS}$ AND $D_{MAX}$

The upper-limit function from Mugele and Evans (1951)

$$y = \ln [a D / (D_{MAX} - D)] \quad (D1)$$

is applied to correlate experimental data of sprays according to the following procedure:

$D_{MAX}$  is determined by trial and error to give the best alignment of data points when  $\ln [D / (D_{MAX} - D)]$  is plotted against the cumulative volume frequency distribution. It is calculated by the following equation derived from geometric considerations:

$$\frac{D_{MAX}}{D_{50}} = \frac{D_{50} (D_{90} + D_{10}) - 2 D_{90} D_{10}}{D_{50}^2 - D_{90} D_{10}} \quad (D2)$$

At first, the cumulative volume frequency undersize curve is calculated from the experimentally-determined number distribution and the diameters  $D_{10}$ ,  $D_{50}$ ,  $D_{90}$  corresponding respectively to the 10<sup>th</sup>, 50<sup>th</sup>, 90<sup>th</sup> percentile are determined from that curve (Figures D1, D3, D5 and D7. Reported in Equation (D2), these values



give a first good approximation of  $D_{MAX}$ .

The function  $D / (D_{MAX} - D)$  is then plotted against the cumulative volume frequency distribution on a log-probability paper (Figure D2, D4, D6 and D8). New values of  $D_{10}$ ,  $D_{50}$  and  $D_{90}$  are obtained from that curve and the new value of  $D_{MAX}$  is compared to the previous one. That procedure ends up when the two values are comparable within a few percent and just one trial is usually only required.

$D_{VS}$  is then given by:

$$D_{VS} = D_{MAX} / (1 + a \exp(1/4\delta^2)) \quad (D3)$$

where  $a$  is the constant from Equation (D1):

$$a = (D_{MAX} - D_{50}) / D_{50} \quad (D4)$$

and  $\delta$ , the size distribution parameter, is determined by the slope of the line:

$$\delta = 0.394 / \log(D_{90} (D_{MAX} - D_{50}) / D_{50} (D_{MAX} - D_{10})) \quad (D5)$$

This is applied in the following pages to the four experimental drop size distributions that have been determined.

Solution 30% NaNO<sub>3</sub>

From Figure D1,

$$D_{10} = 40 \text{ } \mu\text{m}, \quad D_{50} = 94 \text{ } \mu\text{m}, \quad D_{90} = 146 \text{ } \mu\text{m}$$

Reported in Equation (D2), it gives  $D_{MAX} = 182 \text{ } \mu\text{m}$

From Figure D2,

$$D_{10} = 40 \text{ } \mu\text{m}, \quad D_{50} = 92.5 \text{ } \mu\text{m}, \quad D_{90} = 142 \text{ } \mu\text{m}$$

and  $D_{MAX} = 176 \text{ } \mu\text{m}$

$\Delta(D_{MAX}) = 3\%$ , so that we retain that last value for  $D_{MAX}$ .

$$a = 0.67, \quad \delta = 0.78 \text{ and } \underline{D_{VS} = 73 \text{ } \mu\text{m}}$$

Solution 10% NaNO<sub>3</sub>

From Figure D3,

$$D_{10} = 68 \text{ } \mu\text{m}, \quad D_{50} = 118 \text{ } \mu\text{m}, \quad D_{90} = 167 \text{ } \mu\text{m}$$

These values reported in Equation (D2) give  $D_{MAX} = 231 \text{ } \mu\text{m}$

From Figure D4,

$$D_{10} = 66 \text{ } \mu\text{m}, \quad D_{50} = 118 \text{ } \mu\text{m}, \quad D_{90} = 168 \text{ } \mu\text{m}$$

and  $D_{MAX} = 226 \text{ } \mu\text{m}$

$\Delta(D_{MAX}) = 2\%$  and we retain that last value of the maximum stable droplet diameter.

$$a = 0.92, \quad \delta = 0.93 \quad \text{and} \quad \underline{D_{VS} = 101 \mu\text{m}}$$

#### Pure Water - Nozzle Monarch

From Figure D5,

$$D_{10} = 75 \mu\text{m}, \quad D_{50} = 132 \mu\text{m}, \quad D_{90} = 188 \mu\text{m}$$

These values reported in Equation (D2) give  $\underline{D_{MAX} = 259 \mu\text{m}}$

From Figure D6

$$D_{10} = 67 \mu\text{m}, \quad D_{50} = 135 \mu\text{m}, \quad D_{90} = 199 \mu\text{m}$$

$$\text{and} \quad \underline{D_{MAX} = 255 \mu\text{m}}$$

$\Delta(D_{MAX}) = 1.5\%$  so that we retain that last value for the maximum stable droplet diameter.

$$a = 0.89, \quad \delta = 0.79 \quad \text{and} \quad \underline{D_{VS} = 110 \mu\text{m}}$$

#### Pure Water - Nozzle LNN2

From Figure D7

$$D_{10} = 49 \mu\text{m}, \quad D_{50} = 92 \mu\text{m}, \quad D_{90} = 134 \mu\text{m}$$

These values reported in Equation (D2) give  $\underline{D_{MAX} = 180 \mu\text{m}}$

From Figure D8

$$D_{10} = 46 \mu\text{m}, \quad D_{50} = 98 \mu\text{m}, \quad D_{90} = 145 \mu\text{m}$$

and  $\underline{D_{\text{MAX}} = 180 \mu\text{m}}$

which is finally retained.

  $0.84, \quad \delta = 0.73 \text{ and } \underline{D_{\text{VS}} = 77 \mu\text{m}}$

FIGURE D1

CUMULATIVE VOLUME FREQUENCY UNDERSIZE CURVE

CALCULATED FROM FIGURE 14, PART I) FOR 30% NaNO<sub>3</sub> SOLUTION

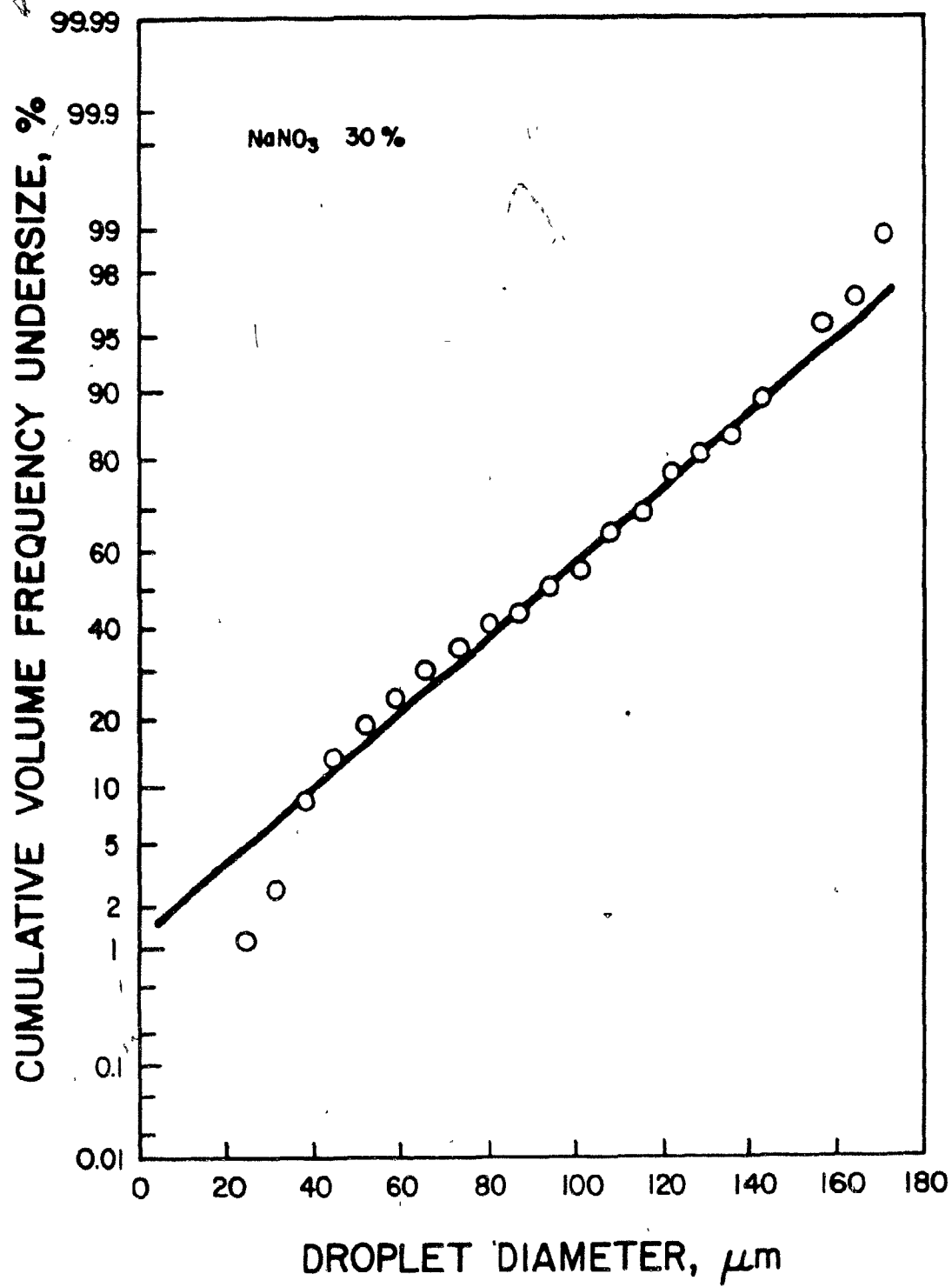


FIGURE D2

FUNCTION D1 ( $D_{\text{MAX}}$ -D) VERSUS CUMULATIVE VOLUME FREQUENCY

UNDERSIZE FOR 30% NaNO<sub>3</sub> SOLUTION

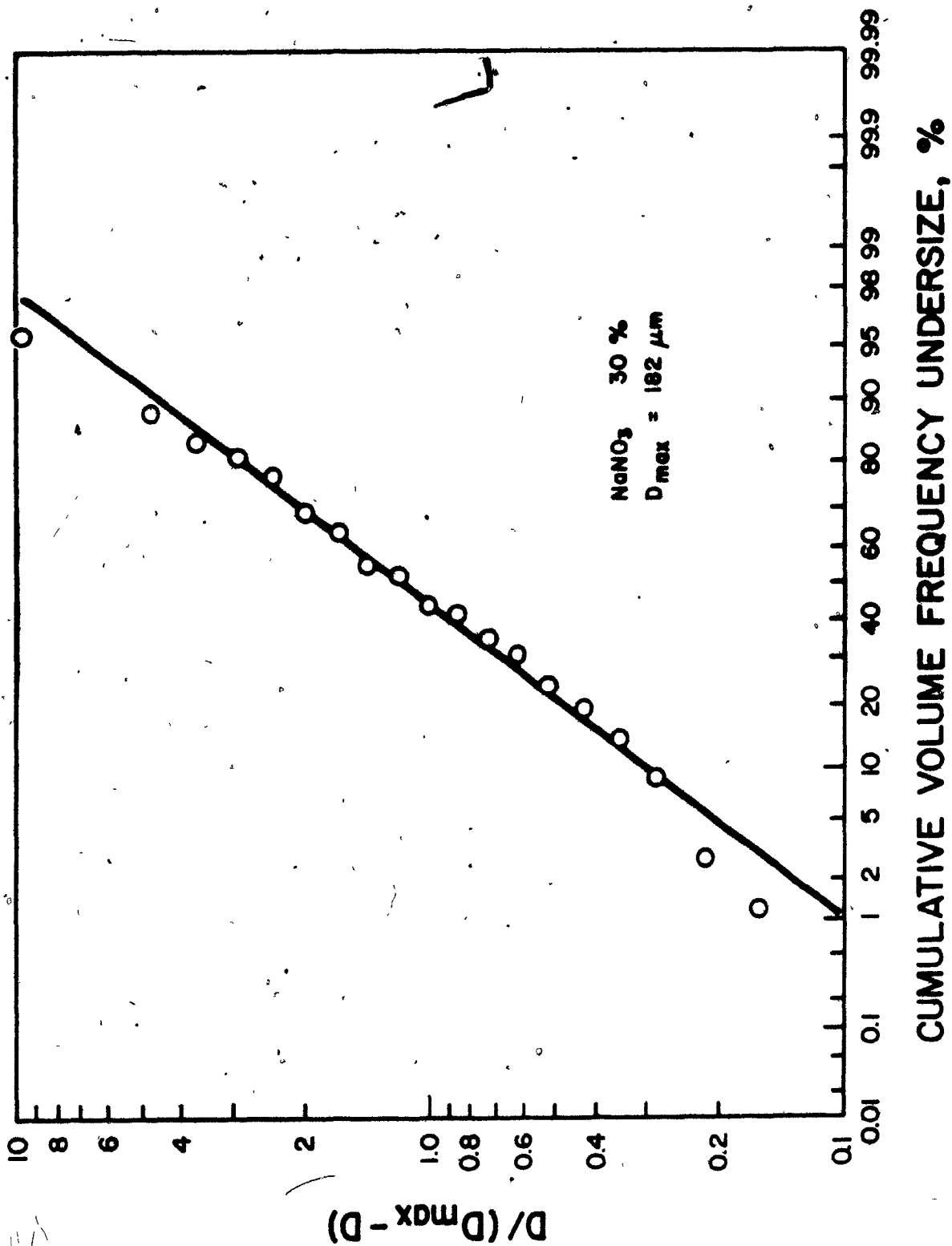




FIGURE D3

CUMULATIVE VOLUME FREQUENCY UNDERSIZE CURVE

CALCULATED FROM FIGURE 17, ROOT 1) FOR 10%  $\text{NaNO}_3$  SOLUTION

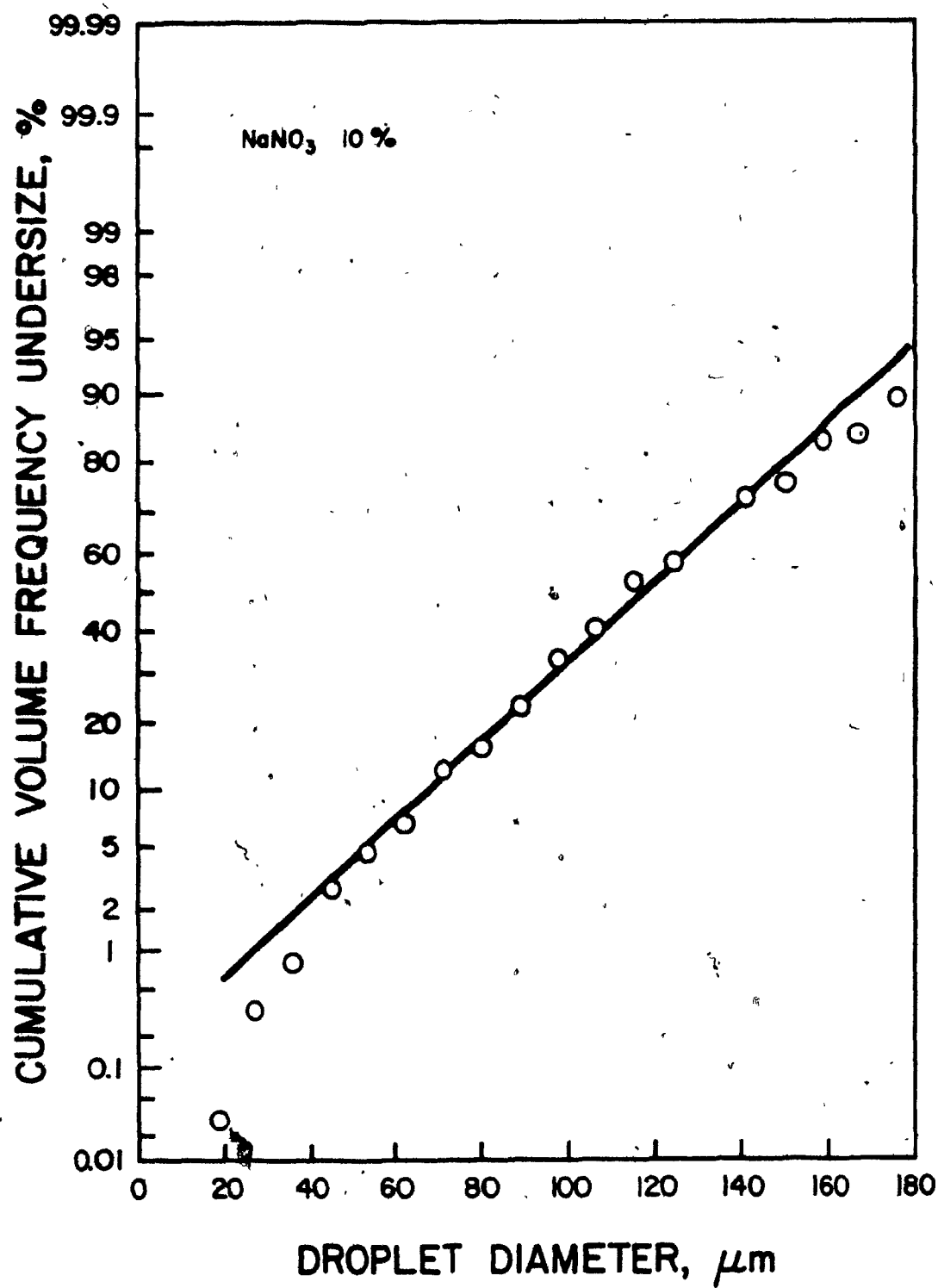


FIGURE D4

FUNCTION  $D/(D_{\text{MAX}} - D)$  VERSUS CUMULATIVE VOLUME FREQUENCY

UNDERSIZE FOR 10% NaNO<sub>3</sub> SOLUTION

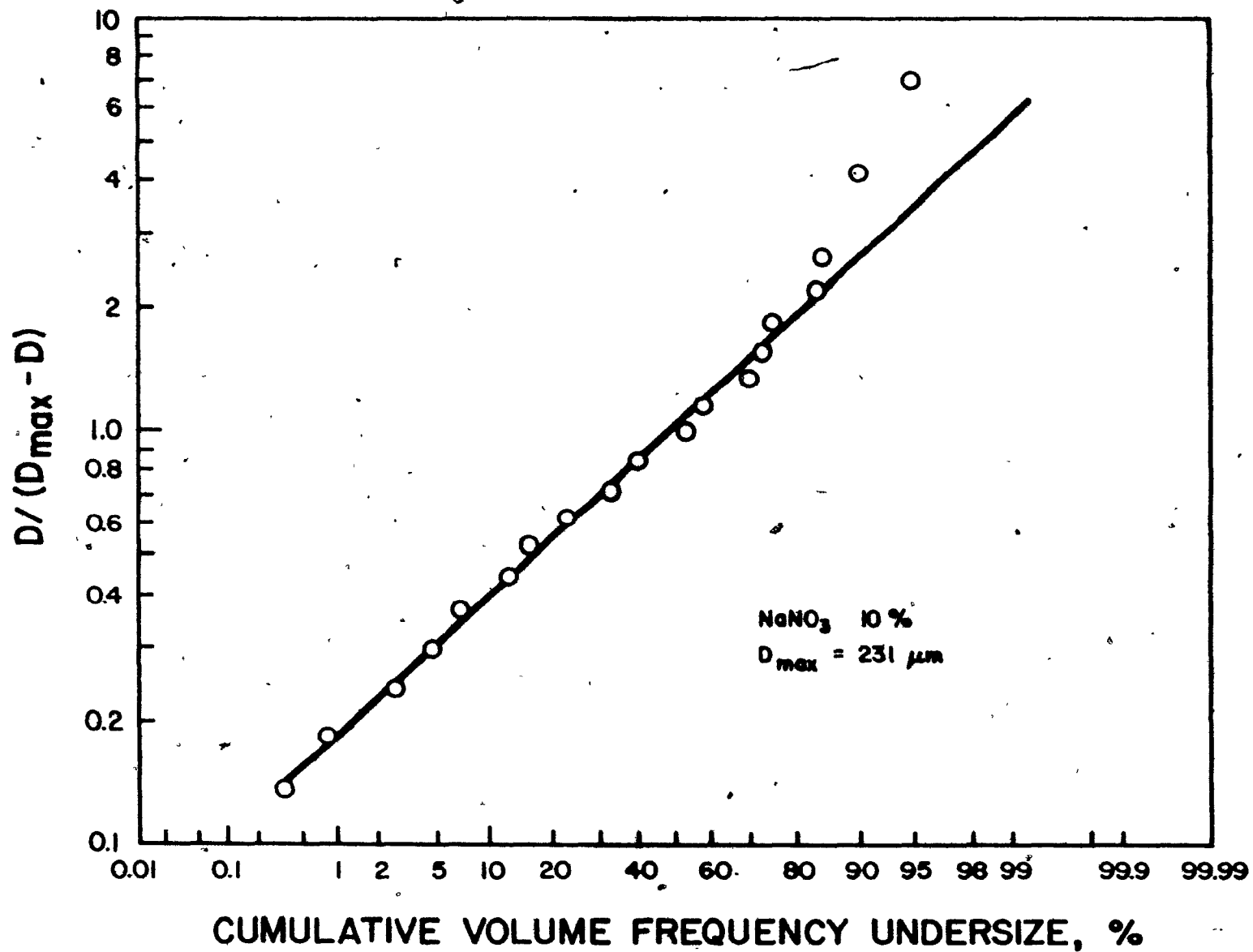


FIGURE D5

CUMULATIVE VOLUME FREQUENCY UNDERSIZE CURVE

CALCULATED FROM FIGURE 20, PART I) FOR WATER, MONARCH 4.0 NOZZLE

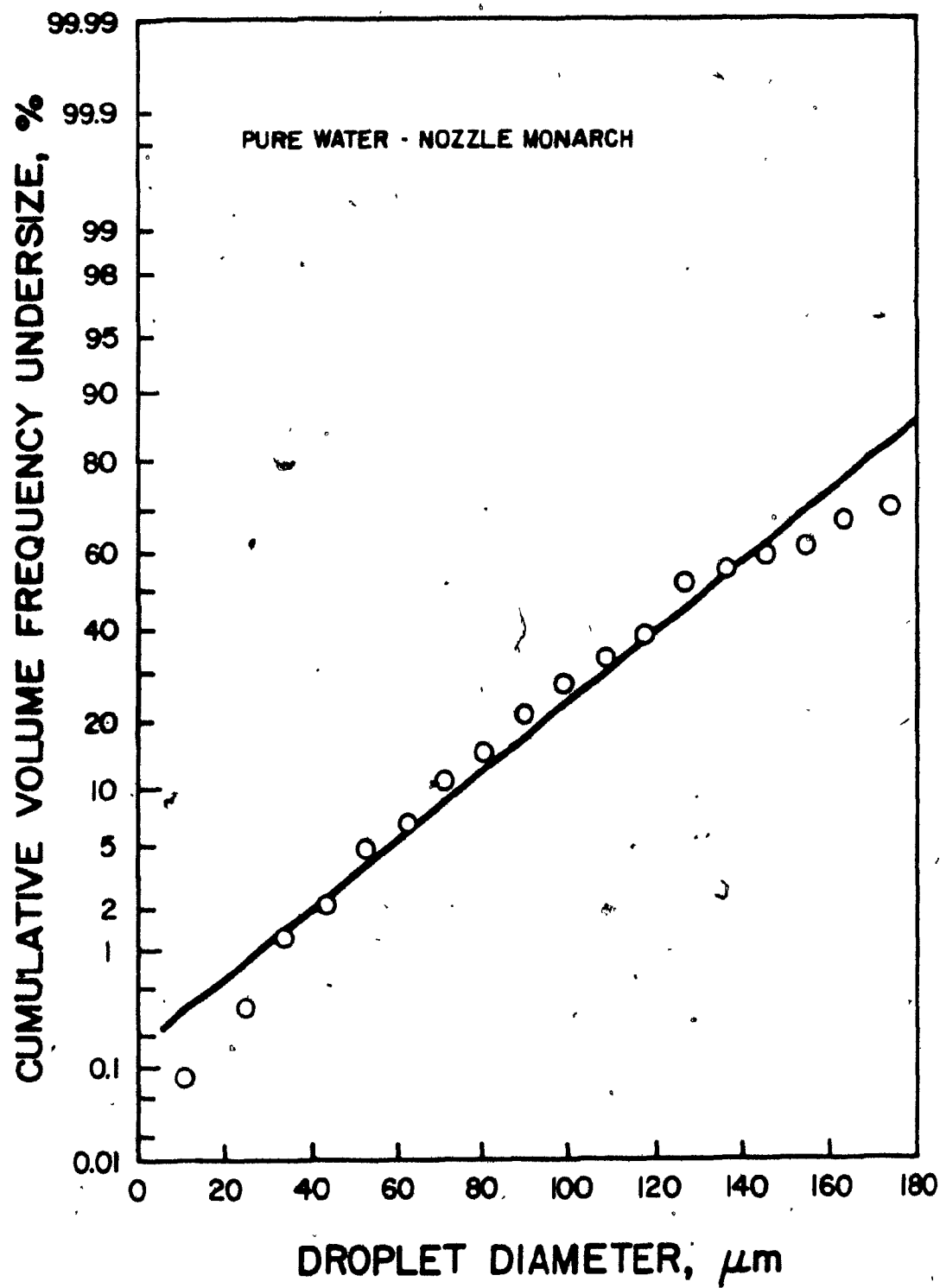


FIGURE D6

FUNCTION  $D/(D_{MAX}-D)$  VERSUS CUMULATIVE VOLUME FREQUENCY

UNDERSIZE FOR WATER, MONARCH 4.0 NOZZLE

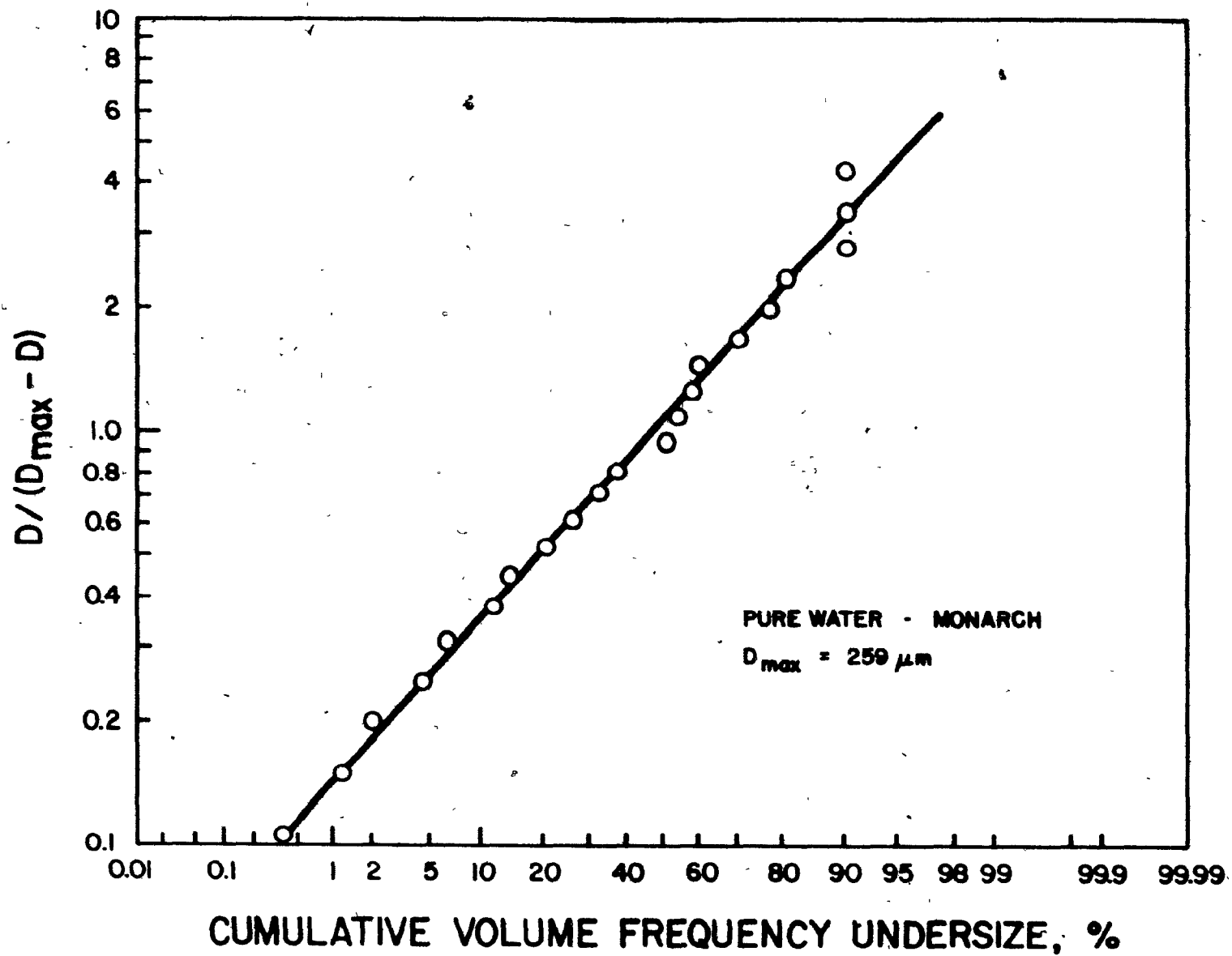




FIGURE D7

CUMULATIVE VOLUME FREQUENCY UNDERSIZE CURVE

CALCULATED FROM FIGURE 24, PART I) FOR WATER, LNN2 NOZZLE

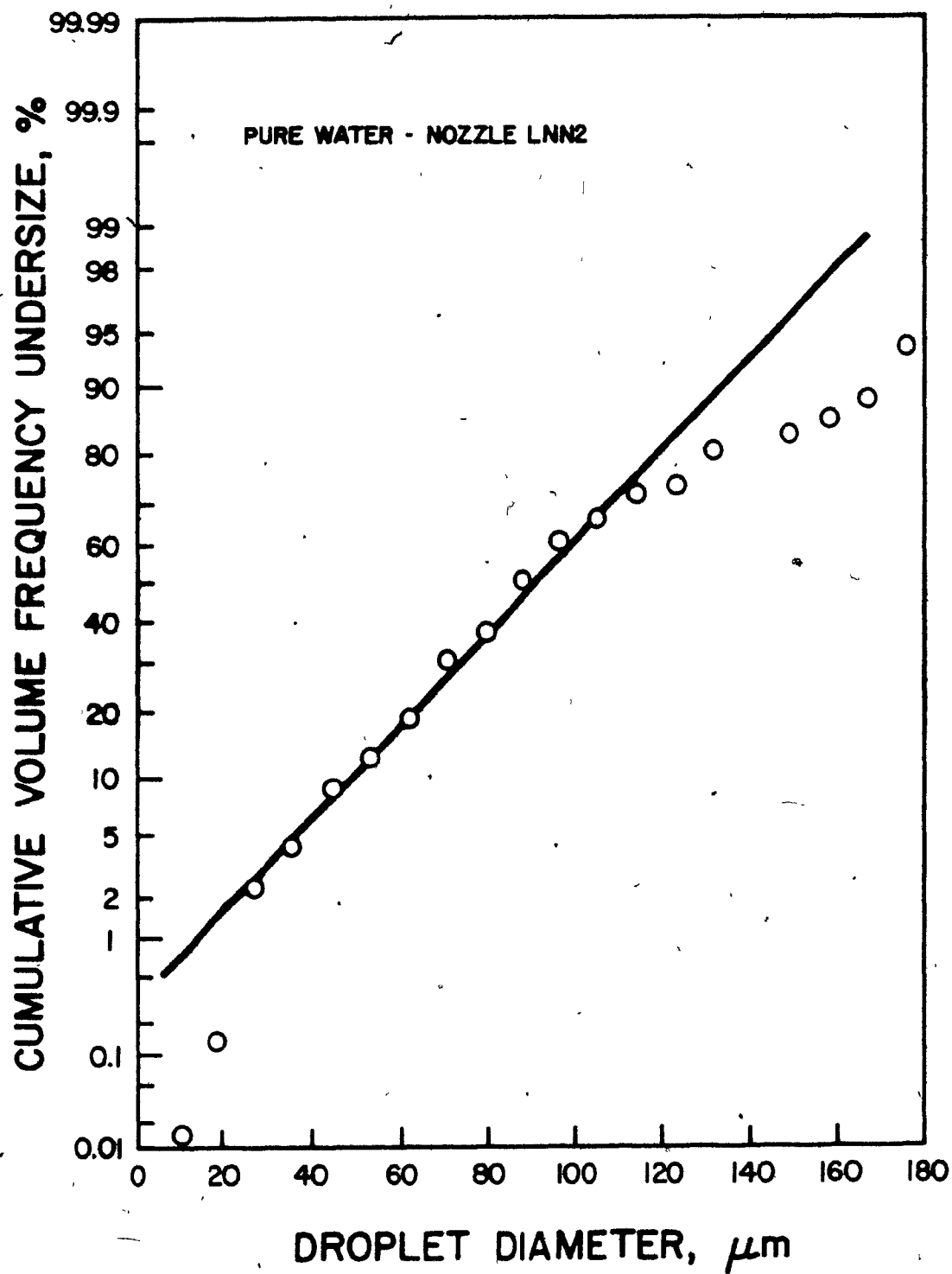
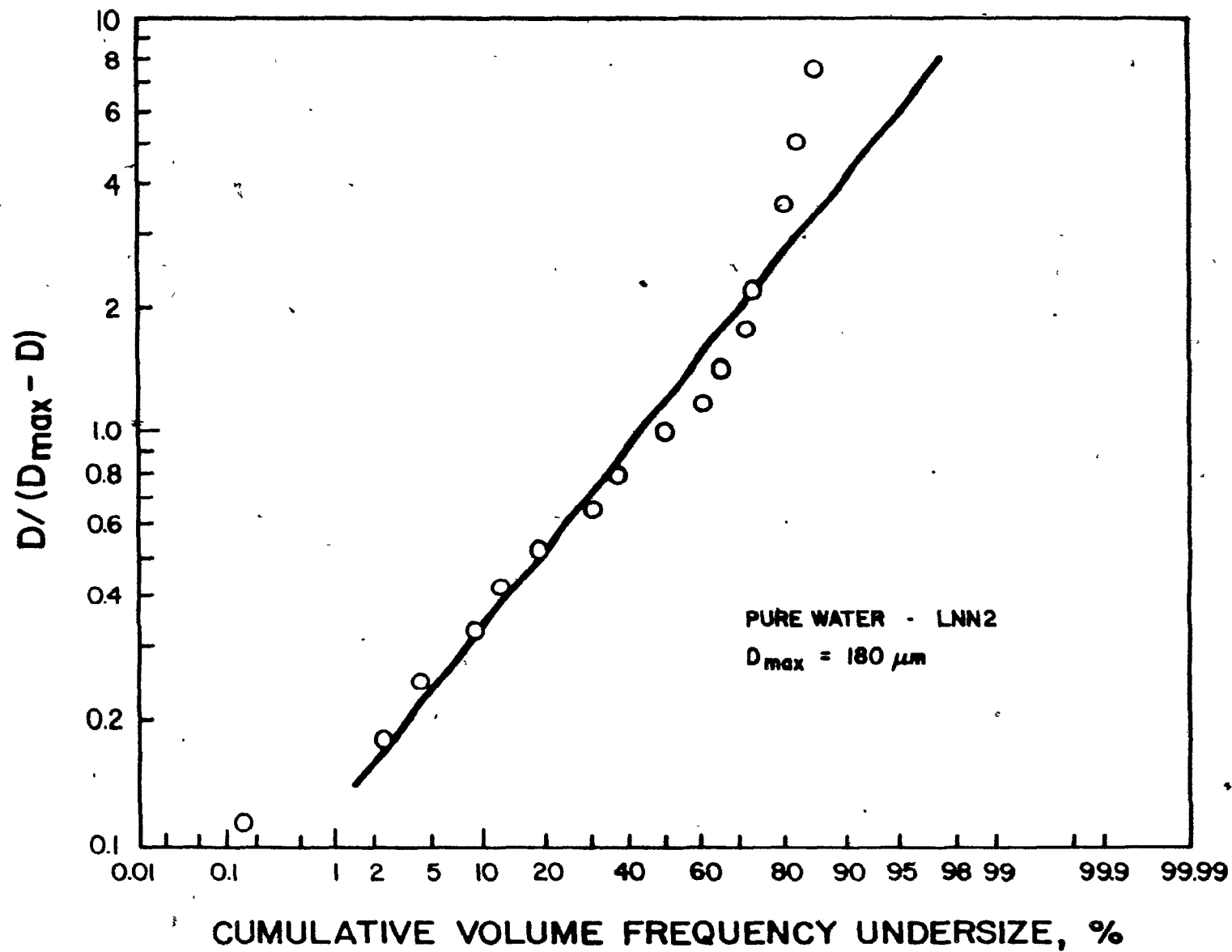


FIGURE D8

FUNCTION  $D/(D_{\text{MAX}} - D)$  VERSUS CUMULATIVE VOLUME FREQUENCY

UNDERSIZE FOR WATER, LNN2 NOZZLE



APPENDIX E

PROGRAM LISTING

APPENDIX E

PROGRAM LISTING

## \*\*\*\*\* VARIABLE LIST \*\*\*\*\*

AINI - INLET DROPLET DIAMETER (M)  
 ALH - LATENT HEAT OF VAPORIZATION AT TW (J/KG)  
 ANUMBR - INITIALLY, VOLUME FRACTION OF FEED PER CLASS  
           RESET AS NUMBER OF DROPLETS IN CLASS (1/S)  
 AREA - INSTANTANEOUS CROSS-SECTIONAL AREA OF CHAMBER (M\*\*2)  
 B - CONSTANT  
 C - DROPLET SOLID CONCENTRATION - WEIGHT FRACTION  
 C1, C4, C5, C6, C7, C8 - CONSTANTS  
 CF - CORRECTION FACTOR FOR HIGH TEMPERATURE OF STEAM  
 CI - INLET DROPLET SOLID CONCENTRATION - WEIGHT FRACTION  
 CON - CONSTANT  
 CPS - HEAT CAPACITY OF SOLID (J/KG/K)  
 CS1 - HEAT CAPACITY OF STEAM (J/KG/K)  
 CURL - CURL OF FLUID VELOCITY (1/S)  
 D - DROPLET DIAMETER (M)  
 DELT - TIME INCREMENT (S)  
 DENA - DENSITY OF STEAM (KG/M\*\*3)  
 DENP - DENSITY OF FEED SOLUTION (KG/M\*\*3)  
 DHA - FUNCTION TO CALCULATE TEMPERATURE FROM ENTHALPY  
 DN - DIAMETER OF NOZZLE (M)  
 DQ - HEAT TRANSFER RATE (W)  
 DT - CHANGE IN T (S)  
 DTH - CHANGE IN ANGULAR POSITION OF CLASS (RADIAN)  
 DVR - CHANGE IN RADIAL VELOCITY OF DROPLET (M/S)  
 DVT - CHANGE IN TANGENTIAL VELOCITY OF DROPLET (M/S)  
 DVV - CHANGE IN AXIAL VELOCITY OF DROPLET (M/S)  
 DWVI - WATER EVAPORATED FROM ONE CLASS OVER TIME DT (KG/S)  
 DXR - CHANGE IN RADIAL POSITION OF DROPLET (M)  
 DXV - CHANGE IN AXIAL POSITION OF DROPLET (M/S)  
 ENZ - AXIAL POSITION OF START OF FREE ENTRAINMENT ZONE (M)  
 EVAP, EX - AMOUNT EVAPORATED (KG/S)  
 EXPC - PERCENT OF CHAMBER CAPACITY USED  
 FCURL - TEMPORARY VARIABLE  
 FLIFT - LIFT FORCE PER MASS OF CLASS (M/S\*\*2)  
 H - ENTHALPY OF ENTRAINMENT STEAM CALCULATED UPON ITS  
       ENTRAINMENT (J/S)  
 HI - FUNCTION TO CALCULATE ENTHALPY FROM TEMPERATURE  
 H11 - ENTHALPY OF FEED AT ENTRANCE TEMPERATURE TE (J/KG)  
 H12 - ENTHALPY OF FEED AT BOILING TEMPERATURE TW (J/KG)  
 HIN - ENTHALPY OF INLET PLASMA STEAM (J/KG)  
 HOUT - ENTHALPY OF OUTLET STEAM AT T2 (J/KG)  
 HI - ENTHALPY OF RECIRCULATING STEAM (J/KG)  
 HTO - ENTHALPY OF RECIRCULATING STEAM AT NOZZLE LEVEL (J/KG)  
 HVB - ENTHALPY OF STEAM AT BOILING POINT TW (J/KG)  
 ICLAS - SMALLEST CLASS OF DROPLETS NOT YET DRY  
 ISIZE - FIRST CLASS WITH VELOCITY PROFILE SIGNIFICANTLY  
           DIFFERENT FROM SSTEAM'S ( $6.5E-5$  TO  $9.5E-5$  M)  
 IT - COUNTER FOR PRINT OUTPUT  
 KO - THERMAL CONDUCTIVITY AT ZERO PRESSURE (W/M/K)  
 KS - THERMAL CONDUCTIVITY OF STEAM (W/M/K)  
 MM - COUNTER . H OF LOOPS BEFORE PRINTING  
 NN - NUMBER OF CLASSES  
 NP - NUMBER OF CLASSES LESS ONE  
 NU - NUSSELT NUMBER  
 P - LIQUID PRESSURE AT NOZZLE (NT/M\*\*2)  
 PI - 3.14....  
 PR - PRANDTL NUMBER

|   |       |                                                                                                 |
|---|-------|-------------------------------------------------------------------------------------------------|
| C | PRF   | - PR TO THE 1/3 POWER                                                                           |
| C | Q     | - HEAT GAIN OF PARTICLES AND THEIR MOISTURE (W)                                                 |
| C | QD    | - VOLUMETRIC FLOWRATE OF STEAM (M**3/S)                                                         |
| C | QL    | - HEAT LOSSES (W)                                                                               |
| C | QLX   | - HEAT LOSSES FRACTION AT X (W)                                                                 |
| C | RS    | - VELOCITY HALF RADIUS (M)                                                                      |
| C | RE    | - REYNOLDS NUMBER                                                                               |
| C | RHO   | - FUNCTION TO CALCULATE STEAM DENSITY FROM TEMPERATURE                                          |
| C | RN    | - RADIUS OF NOZZLE (M)                                                                          |
| C | RX    | - RADIUS OF CHAMBER (M)                                                                         |
| C | T     | - TIME EACH CLASS IS IN DRYER (S)                                                               |
| C | T1    | - INLET TEMPERATURE (K)                                                                         |
| C | T2    | - OUTLET TEMPERATURE (K)                                                                        |
| C | TA    | - TEMPERATURE OF DRYING STEAM IN NOZZLE ZONE (K)                                                |
| C | TAF   | - FILM TEMPERATURE - AVERAGE OF TA AND TW (K)                                                   |
| C | PH    | - ANGULAR POSITION OF CLASS OF DROPLETS (RADIAN)                                                |
| C | THETA | - ANGLE OF SPRAY WITH CENTERLINE (RADIAN)                                                       |
| C | TIME  | - TIME STEAM IS IN DRYER (S)                                                                    |
| C | TS    | - TEMPERATURE OF UNENTRAINED STEAM SURROUNDING THE JET                                          |
| C | TT    | - TEMPERATURE OF RECIRCULATING STEAM (K)                                                        |
| C | TTD   | - TEMPERATURE OF RECIRCULATING STEAM AT NOZZLE LEVEL (K)                                        |
| C | TW    | - BOILING POINT OF EVAPORATING VAPOR (K)                                                        |
| C | VAC   | - VELOCITY OF STEAM ON THE CENTERLINE OF THE DRYER (M/S)                                        |
| C | VAR   | - RADIAL VELOCITY OF STEAM (M/S)                                                                |
| C | VAT   | - TANGENTIAL VELOCITY OF STEAM (M/S)                                                            |
| C | VAV   | - AXIAL VELOCITY OF STEAM (M/S)                                                                 |
| C | VAW   | - AXIAL VELOCITY OF STEAM WITHOUT JET 'S EFFECT (M/S)                                           |
| C | VD    | - INITIAL VELOCITY OF DROPLET (M/S)                                                             |
| C | VDD   | - TEMPORARY VARIABLE IN AXIAL VELOCITY EQUATION                                                 |
| C | VF    | - VELOCITY OF DROPLET RELATIVE TO THAT OF FLUID (M/S)                                           |
| C | VISA  | - VISCOSITY OF STEAM (KG/M/S)                                                                   |
| C | VN    | - AVERAGE VELOCITY OF LIQUID LEAVING ATOMIZER (M/S)                                             |
| C | VR    | - DROPLET RADIAL VELOCITY (M/S)                                                                 |
| C | VT    | - DROPLET TANGENTIAL VELOCITY (M/S)                                                             |
| C | VV    | - DROPLET AXIAL VELOCITY (M/S)                                                                  |
| C | WAA   | - MASS FLOW RATE OF ATOMIZING STEAM (KG/S)                                                      |
| C | WD    | - MASS FLOW RATE OF CLASS OF DROPLETS (KG/S)                                                    |
| C | WE    | - MASS FLOW RATE OF ENTRAINED STEAM (KG/S)                                                      |
| C | WEP   | - PREVIOUS VALUE OF ENTRAINMENT                                                                 |
| C | WG    | - FLOW RATE OF PLASMA STEAM (KG/S)                                                              |
| C | WIN   | - MASS FLOW RATE OF RECIRCULATING STEAM AT NOZZLE LEVEL                                         |
| C | WL    | - MASS FLOW RATE OF FEED (KG/S)                                                                 |
| C | WOUT  | - MASS FLOW RATE OF RECIRCULATING STEAM IN THE FREE<br>ENTRAINMENT ZONE                         |
| C | WP    | - WEIGHT OF PRODUCT THAT IS DRY (KG/S)                                                          |
| C | WS    | - WEIGHT OF SOLID IN EACH CLASS (KG/S)                                                          |
| C | WSP   | - MASS FLOW RATE OF SOLID PRODUCT                                                               |
| C | WT    | - MASS FLOW RATE OF RECIRCULATING STEAM AT ANY POINT XV                                         |
| C | WW    | - CHAMBER CAPACITY (KG WATER EVAP. /S)                                                          |
| C | WWI   | - NON-EVAPORATED WATER IN EACH CLASS (KG/S)                                                     |
| C | XP    | - LENGTH OF LIQUID SHEET AT NOZZLE EXIT (M)                                                     |
| C | XPP   | - MAXIMUM DISTANCE BEYOND WHICH THE ENTRAINMENT VARIES<br>NO MORE LINEARLY WITH DISTANCE XV (M) |
| C | XR    | - RADIAL POSITION OF DROPLET (M)                                                                |
| C | XRM   | - RADIUS OF ANNULAR REGION IN NOZZLE ZONE (M)                                                   |
| C | XV    | - AXIAL POSITION OF DROPLET (M)                                                                 |



C DESIGN OF A SPRAY-DRYER : CO-CURRENT TYPE ,ATOMIZING PRESSURE  
 C NOZZLE ; PLASMA-GENERATED SUPERHEATED STEAM AS THE DRYING MEDIUM .  
 C PROGRAMMED BY S. KATTA -1975-  
 C MODIFIED BY H. COSTIN -1978-  
 C MODIFIED BY M.P. AMLOT -1982-  
 C

```

    IMPLICIT REAL*8 (A-H,O-Z)
    COMMON /BLOCK1/CS1
    DIMENSION VU(25),VR(25),VT(25),VF(25),XR(25),TH(25),RY(25),
    ID(25),T(25),AINI(25),ANUMBR(25),WWI(25),WS(25),C(25)
    DIMENSION TITLE (20)
    REAL*8 NU,K9,KO
    EQUIVALENCE (EVAP,EX), (B,CF)
    DATA IT/1/,ICLAS/1/,TIME/0.00/,EVAP/0.00/,WP/0.00/,WE/0.00/
    READ (5,999) (TITLE(N),N=1,11)
    READ(5,105) ISIZE,MM,NN
    60  FORMAT(6X,F8.3,E11.3,1X,3F0.3,2X,2F7.3,F7.2,F7.2)
    65  FORMAT(/,F7.3,1X,F6.3,65X,2F8.5,2X,2F6.0,F7.3,F7.3,1X,F5.1)
    73  FORMAT(D10.3,F14.4)
    63  FORMAT('OHEAT LOSSES =',F10.0,'W',///,' MASS FLOW RATE PLASMA '
    1,'STEAM =',F7.5,' KG/S',///,' POWER REQUIRED BY PLASMA =',
    HF10.0,'W',////)
    93  FORMAT(6D10.3)
    100  FORMAT(1H1,2X,'XV',4X,'TIME',6X,'D',1:1X,'VU',6X,'VR',6X,'VT',7X,
    1'VF',5X,'XR',5X,'RE',6X,'C ',3X,'WE',6X,'WT',7X,'TA',4X,'IS',6X,
    2'EX',4X,'WAV',3X,'EXP')
    READ(5,93) HIN,T2,T1,TW
    105  FORMAT(3I5)
    300  FORMAT(/,F14.3,6X,'GROUP',2X,I6,2X,'(',2X,F10.3,2X,'METERS) IS
    1'DRY')
    550  FORMAT(/,20X,A24,/)
    999  FORMAT(11A4)
    998  FORMAT(/,11A4,/)
    95  FORMAT(5D7.2)
    75  FORMAT(//,'DT1=',D9.2,'TIME1=',D9.2,
    1H/,'DT2=',D9.2,'TIME2=',D9.2,
    2  /,'DT3=',D9.2,/)
    READ(5,93) DN,RX,THETA,CPS
    READ(5,73) DENP,PR,VN
    READ(5,93) WAA,WL,C1,XV
    READ(5,73) (D(I),ANUMBR(I),I=1,NN)
    READ(5,73) ALH
    READ(5,73) P
    READ(5,73) ALOST
    READ(5,73) TE
    READ(5,73) ENZ
    READ(5,95) DT1,TIME1,DT2,TIME2,DT3
    WRITE(6,998) (TITLE(N),N=1,11)
    WRITE(6,105) ISIZE,MM,NN
    WRITE(6,93) HIN,T2,T1,TW
    WRITE(6,93) DN,RX,THETA,CPS
    WRITE(6,93) DENP,PR,VN
    WRITE(6,93) WAA,WL,C1,XV
    WRITE(6,73) (D(I),ANUMBR(I),I=1,NN)
    WRITE(6,73) ALH
    WRITE(6,73) P
    WRITE(6,73) ALOST
    WRITE(6,73) TE
    WRITE(6,73) ENZ
    WRITE(6,75) DT1,TIME1,DT2,TIME2,DT3
  
```

```

IT=IT+NN-ICLAS+3
GO TO 80
71 WRITE(6,550) 'PARTICLES HIT THE WALLS'
GO TO 70

```

```

C GO TO 70
72 WRITE(6,550) 'LARGEST PARTICLE IS DRY'
70 WRITE(6,60)(I(I),D(I),VV(I),VR(I),VT(I),VF(I),XR(I),RY(I),C(I),
+I=ICLAS,NN)
EXPC=EX*100.00/WW
WRITE(6,65)XV,TIME,WE,WT,TA,TS,EVAP,VAV,EXPC
WRITE(6,63)QL,WG,Q
STOP
END

```

```

C
C
C

```

FUNCTION TO CALCULATE STEAM ENTHALPY . BASE 1000K.

```

C DOUBLE PRECISION FUNCTION H1(T)
C IMPLICIT REAL*8(A-Z)
C COMMON /BLOCK1/CS1
C DATA H1000/-1.1992D7/
C IF(T.LT.1000.) GO TO 40
C IF(T.LE.2700.) GO TO 10
C IF(T.GT.3500.) GO TO 20
C H1=(-15.835+5.2D-3*T)*4.1868D6-H1000
C RETURN
10 H1=(-3.501+6.37D-4*T)*4.1868D6-H1000
C RETURN
20 H1=(-33.6065+1.046D-2*T)*4.1868D6-H1000
C RETURN
40 H1=(T-1000.)*CS1
C RETURN
C END

```

```

C
C
C

```

FUNCTION TO CALCULATE STEAM TEMPERATURE FROM ITS ENTHALPY

```

C DOUBLE PRECISION FUNCTION DHA(H)
C IMPLICIT REAL*8(A-Z)
C COMMON /BLOCK1/CS1
C DATA H1000/-11.992D6/
C IF(H.LT.0.) GO TO 40
C H1=(H+H1000)/4.1868D6
C IF(H1.LT.-1.779) GO TO 10
C IF(H1.GT.1.7338) GO TO 20
C DHA=(H1+15.835)/5.2D-3
C RETURN
10 DHA=(H1+3.501)/6.37D-4
C RETURN
20 DHA=(H1+33.6065)/1.046D-2
C RETURN
40 DHA=H/CS1+1000.
C RETURN
C END

```

```

C
C
C

```

FUNCTION TO CALCULATE STEAM DENSITY . IDEAL GAS ASSUMED .

```

C DOUBLE PRECISION FUNCTION RHO(T)
C IMPLICIT REAL*8(A-Z)
C CALCULATING NUMBER OF MOLES PER MOLL STEAM
C RHO=1.
C IF(T.LT.2500.) GO TO 10
C RHO=RHO*2.*(T-2600.)/2400.
C IF(RHO.GT.3.) RHO=3.

```

```

DQ=PI*KS*NU*D(I)*ANUMBR(I)*(TA-TW)*DT*CF
DWWI=DQ/ALH
IF(DWWI.GT.WWI(I)) DWWI=WWI(I)
EX=EX+DWWI
WWI(I)=WWI(I)-DWWI
C(I)=WS(I)/(W(I)+WWI(I))
DLNP=0.92203+0.92203*C(I)
D(I)=C4*AINI(I)/(DLNP*C(I))*0.33333
200 F(I)=F(I)+DT
XV=XV+DXV
IF(TIME.GE.TIME1) DELT=DT2
IF(TIME.GE.TIME2) DELT=DT3
TIME=TIME+DELT
C
C CHECK IF PARTICLES HIT WALL OR IF LARGEST PARTICLE IS DRY
C
C IF(XR(NN).GE.RX) GO TO 71
C IF(C(NN).GE..99900) GO TO 72
C IF(ICLAS.GT.NP) GO TO 290
C
C CHECK IF PARTICLES OF THIS SIZE ARE COMPLETELY DRY
C
C IF(C(ICLAS).LT.1.00) GO TO 290
WRITE(6,500) TIME,ICLAS,AINI(ICLAS)
IT=IT+C
WP=WP+WS(ICLAS)
ICLAS=ICLAS+1
IF(ICLAS.GT.ISIZE) ISIZE=ICLAS
290 Q LX=(EX/WW)*QL
IF(XV.LT.ENL) GO TO 300
C
C HEAT BALANCE IN FREE-ENTRAINMENT ZONE
C
C WOUT=W1
TS=DHA((WG*HIN-WL*(H12-H11))-ALH*EX+EX*HTD-Q LX-WP*(T2-TW)*CPS
I+WOUT*HOUT)/(WG+WOUT+EX)
TA=TS
GO TO 50
C
C HEAT BALANCE IN NOZZLE ZONE
C
300 WEP=WE
IF(XV.GT.XPP) GO TO 51
WE=CC*DSQRT(RHO(TS))*(XV-XP)
GO TO 52
51 WE=C9*DSQRT(RHO(TS))*(XV-XP)
52 WT=WT+WL
TT=1141*XV*XV-31.7*XV+794
HT=H1(T1)
DWE=WE-WEP
H=H+DWE*HT(TS)
TS=DHA((WG*HIN-WL*(H12-H11)+WT*HT-H-Q LX)/(WT+WG-WE))
TA=DHA((H-ALH*EX+EX*HTD-WP*(T2-TW)*CPS)/(WE+EX))
IF(TA.GE.TS) TA=TS
50 CONTINUE
C
C OUTPUT-AFTER HM CALCULATIONS
C
C IF(IT.LT.45) GO TO 240
WRITE(3,100)
IT=1
240 EXPC=EX*100.00/WW
WRITE(6,65) XV,TIME,WE,WT,TA,TS,EVAP,UVU,EXPC
WRITE(6,60) (T(I),D(I),VV(I),VR(I),VT(I),VF(I),XR(I),RY(I),C(I),
II=ICLAS,NN)

```

TAF=(TA+TW)/2.00

DENA=KHO(TAF)

VISA=3.3630-8.4TAF +9.570-7

K0=6.47530-3\*DSQRT(TAF)/(1.00+1737.300/TAF+10.00\*(-12./TAF))

KS=K0+4.5930-3\*(10.00\*(0.93409/TAF\*\*4)-1.00)

GG=(WG\*EX)/KH0(TA)

CON=DTAN(THETA/2)

R5=CON\*XV

VAC=CS/XV

DXV=VV(ISIZE)\*DELT

B=(TA-TW)\*CS/ALH

CF=DLOG(B(1.00))/B

C

C PREDICTION OF TRAJECTORIES OF DROPLETS OF ALL SIZES

C

DO 200 I=ICLAS,NN

DT=DXV/VV(I)

DENP=0.9203 +0.9203\*C(I)

VAV=VAC\*DEXP(-0.69200\*(XR(I)/R5)\*\*2)

IF(XV.LT.ENZ) GO TO 140

VAV=GG/AREA

VAV=VAV +VAV

140 VF(I)=DSQRT((VT(I)-VAT)\*\*2 +(VV(I)-VAV)\*\*2 +(VR(I)-VAR)\*\*2)

IF(I.GE.ISIZE) GO TO 25

VV(I)=VAV

VR(I)=VAR

VT(I)=VAT

NU=2.00

GO TO 15

25

RE=D(I)\*VF(I)\*DENA/VISA

C

C CALCULATING CD FROM BEARD AND PRUPPACHER EQUATIONS

C

IF(RE.LE.0.200) CD=24.00/RE

IF(RE.LE.2.00.AND.RE.GT.0.200)

1 CD=24.00\*(1.00+0.100\*RE\*\*0.9900)/RE

IF(RE.LE.21.00.AND.RE.GT.2.00)

1 CD=24.00\*(1.00+.1100\*RE\*\*0.8100)/RE

IF(RE.LE.200.00.AND.RE.GT.21.00)

1 CD=24.00\*(1.00+0.10900\*RE\*\*0.63200)/RE

IF(RE.GT.200.00)

1 CD=24.00\*(1.00+.1500\*RE\*\*0.68700)/RE

VDD=0.7500\*CD\*DENA\*VF(I)/(DENP\*D(I))

IF(RE.GT.1.00) GO TO 27

CURL=1.400\*VAV\*XR(I)/(R5\*R5)

FCURL=DSQRT(VISA\*CURL/DENA)

FLIFT=C7\*DENA\*FCURL\*VF(I)/(D(I)\*DLNP)

GO TO 28

27

FLIFT=0.00

28

DVV=(9.3100-VDD\*(VV(I)-VAV))\*DT

DVR=(VT(I)\*\*2/XR(I)-VDD\*(VR(I)-VAR)+FLIFT)\*DT

DV=(-VF(I)\*VR(I)/XR(I)-VDD\*(VT(I)-VAT))\*DT

15

DXR=VR(I)\*DT

DTH=VT(I)\*DT/XR(I)

IF(I.LT.ISIZE) GO TO 22

VV(I)=VV(I) +DVV

VR(I)=VR(I) +DVR

VT(I)=VT(I) +DVT

RY(I)=RE

NU=2.00+0.2000\*DSQRT(RE)\*PRF

22

XR(I)=XR(I)+DXR

TH(I)=TH(I)+DTH

C

C CALCULATION OF MASS TRANSFER RATE

C

```

RN=DN/2.
NP=NN-1
PI=4.00*DATAN(1.00)
PR=PR*(1./3)
DEL T=DTI
WSP=WL*CI
WW=WL-WSI
C1=DLOG(.100./1.02)
C4=(DENP*CI)**0.33300
C5=DTAN(THETA)
C6=5.200*VN*DN
CON=4.00*PI/3/8
C7=20.2500/CON
C8=0.9600*DSQRT(WL*VN*C5**2/DCOS(THETA))
C9=0.202*DSQRT(WL*VN)
CS1=2.0103
XP=0.00500+1.5802*DSQRT(WL/DENP/DSQRT(P))*DCOS(THETA)
XPP=10.*XP +0.066

```

C  
C  
C

# INITIALIZATION

```

DO 86 I=1,NN
AINI(I)=D(I)
VD=2.004*D(I) +6.00
VV(I)=VD*DCOS(THETA)
VR(I)=VD*DSIN(THETA)
VT(I)=0.00
XR(I)=(RN*XV*C5)*I/NN
TH(I)=0.00
T(I)=0.00
C(I)=CI
RY(I)=0.00
WD=WL*ANUMDR(I)
ANUMDR(I)=WD/(CON*DENP*(D(I)**3))
WWI(I)=WD*(1.00-CI)
WS(I)=WD*CI

```

86  
C  
C  
C

# OVERALL HEAT BALANCE

```

Q=WW*ALH +CS1*WW*(T2-TW) +WSP*CPS*(T2-TW)
H11=0.21706*CI +0.04206
IF(TE.EQ.TW) GO TO63
H12=-0.36006*CI +0.49906
GO TO 61
H12=H11
Q=Q+WL*(H12-H11)
QL=ALOST*Q
HTB=H1(TW)
HOUT=H1(T2)
Q=Q+QL
WG=Q/(HIN-HOUT)
TA=860.00
TS=TA
AREA=PI*RX*RX
TTO=794.00
HTO=H1(TTO)
WIN=(HIN-H1(TA))*WG/(H1(TA)-HTO)
WRITE(6,100)
VAR=0.00
VAT=0.00
H-U.
WL=0.

```

C  
C  
80

DO 50 JJ=1,MM